Residual stress effects and damage tolerance behaviour of integral lightweight structures manufactured by FSW and HSM

by

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“To design a spacecraft right takes an infinite amount of effort. This is why it’s a good idea to design them to operate when some things are wrong.”

David L. Akin in Akin’s Laws of Spacecraft Design [1]
Abstract

While the civil aviation market is striving for cost reduction and performance enhancement, safety regulations and well established design criteria have to be fulfilled. In this context, a constant search for new manufacturing techniques can be observed. The present thesis aims on providing advanced knowledge in several areas related to the manufacturing of lightweight integral metallic structures with innovative techniques.

Aspects discussed in this thesis include welding monitoring using FBG sensors, residual stress and distortion effects due to FSW, distortion prediction in panels manufactured by HSM and damage tolerance of aircraft skin details manufactured by HSM and FSW.

The broad scope of this thesis provides insight into several different aspects of lightweight integral metallic structures, starting with manufacturing processes and finishing with damage tolerance analyses. In the course of this work, several measurement techniques and devices have been adapted and developed for their application on lightweight integral structures. Such achievements are also included in the appendixes for convenience.

The application of some of the studied concepts in the aeronautical industry is further discussed in the face of an internship at the Airbus Operations GmbH.
Resumo

Enquanto o mercado da aviação civil se esforça para reduzir custos e melhorar o desempenho, regulamentos de segurança e critérios de projecto bem estabelecidos têm de ser cumpridos. Neste contexto, uma constante procura por novos métodos de fabrico pode ser observada. A presente tese pretende fornecer conhecimentos avançados em diversas áreas relacionadas com o fabrico de estruturas metálicas integras leves com processos de fabrico emergentes.

Aspectos discutidos nesta tese incluem a monitorização de soldadura usando sensores FBG, efeitos de tensões residuais e distorção devido ao FSW, previsão da distorção em painéis fabricados utilizando HSM e estudos de tolerância ao dano de detalhes da casca de aviões fabricados por HSM e FSW.

O vasto âmbito desta tese transmite novos conhecimentos relacionados com vários aspectos de estruturas metálicas integras ligeiras, começando com os processos de fabrico até à análise de tolerância ao dano das mesmas. No contexto deste trabalho foram desenvolvidas ou adaptadas várias técnicas de medição e diferentes equipamentos para a aplicação em estruturas metálicas integras leves. Alguns destes aspectos são também apresentados nos anexos da presente tese.

A aplicação de alguns dos conceitos estudados na indústria aeronáutica é finalmente também discutida face ao estágio desenvolvido na empresa Airbus Operations GmbH.
Kurzfassung


Résumé

Alors que le marché de l’aviation civile se bat pour la réduction des coûts et l’amélioration des performances, les critères de sécurité et de conception bien établies doivent être respectés. Dans ce contexte, une recherche constante de nouvelles techniques de fabrication doivent être observées. La présente thèse vise à fournir des connaissances de pointe dans plusieurs domaines liés à la fabrication de structures métalliques intégrals légères avec des technologies innovantes.

Les aspects abordés dans cette thèse incluent la monitorisation des soudures à l’aide de capteurs FBG, des contraintes résiduelles et des effets de distorsion dues à l’FSW, la prédiction de la distorsion dans les panneaux fabriqués par HSM et le tolérance aux dommage des détails de la peau des avions fabriqués par HSM et FSW.

Une partie importante de cette thèse permet de mieux comprendre plusieurs aspects différents des structures métalliques intégrals légères, à commencer par les procédés de fabrication jusqu’à analyse de tolérance aux dommage. Au cours de ce travail, plusieurs techniques et dispositifs de mesure ont été adaptés et développés pour leur application sur des structures intégrals légères. Ces réalisations sont également incluses dans les annexes pour plus de commodité du lecteur.

L’application de certains des concepts étudiés dans l’industrie aéronautique est encore discuté dans le contexte d’un stage à l’Airbus Operations GmbH.
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<tbody>
<tr>
<td>AA</td>
<td>The Aluminium Association</td>
</tr>
<tr>
<td>ANN</td>
<td>Artificial Neural Network</td>
</tr>
<tr>
<td>ASTM</td>
<td>American Society for Testing and Materials</td>
</tr>
<tr>
<td>BEM</td>
<td>Boundary Element Method</td>
</tr>
<tr>
<td>CA</td>
<td>Cyanoacrylate</td>
</tr>
<tr>
<td>CAD</td>
<td>Computer Assisted Design</td>
</tr>
<tr>
<td>CATIM</td>
<td>Centro de Apoio Tecnológico à Indústria Metalomecânica</td>
</tr>
<tr>
<td>CCD</td>
<td>Charge Coupled Device</td>
</tr>
<tr>
<td>CFD</td>
<td>Computational Fluid Dynamics</td>
</tr>
<tr>
<td>CMM</td>
<td>Coordinate Measuring Machine</td>
</tr>
<tr>
<td>CNC</td>
<td>Computer Numerical Controlled</td>
</tr>
<tr>
<td>COINS</td>
<td>COst effective INtegral metallic Structure</td>
</tr>
<tr>
<td>C(T)</td>
<td>Compact Tension</td>
</tr>
<tr>
<td>DBEM</td>
<td>Dual Boundary Element Method</td>
</tr>
<tr>
<td>DEMec</td>
<td>Departamento de Engenharia Mecânica</td>
</tr>
<tr>
<td>DHD</td>
<td>Deep Hole drilling</td>
</tr>
<tr>
<td>DIC</td>
<td>Digital Image Correlation</td>
</tr>
<tr>
<td>DIN</td>
<td>Deutsches Institut für Normung</td>
</tr>
<tr>
<td>DLR</td>
<td>Deutsches Zentrum für Luft- und Raumfahrt</td>
</tr>
<tr>
<td>DoE</td>
<td>Design of Experiment</td>
</tr>
<tr>
<td>EADS</td>
<td>European Aeronautic Defence and Space Company N.V.</td>
</tr>
<tr>
<td>EBSD</td>
<td>Electron Backscattered Diffraction Scan</td>
</tr>
<tr>
<td>EDM</td>
<td>Electro Discharge Machining</td>
</tr>
<tr>
<td>EN</td>
<td>European Standard</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Full Form</td>
</tr>
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<td>--------------</td>
<td>-----------</td>
</tr>
<tr>
<td>EU</td>
<td>European Union</td>
</tr>
<tr>
<td>EWI</td>
<td>Edison Welding Institute</td>
</tr>
<tr>
<td>FBG</td>
<td>Fibre Bragg Grating</td>
</tr>
<tr>
<td>FCG</td>
<td>Fatigue Crack Growth</td>
</tr>
<tr>
<td>FCT</td>
<td>Fundação para a Ciência e Tecnologia (Portuguese Foundation for Science and Technology)</td>
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<tr>
<td>FEA</td>
<td>Finite Element Analysis</td>
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<td>Finite Element Method</td>
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<td>FEUP</td>
<td>Faculdade de Engenharia da Universidade do Porto</td>
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<tr>
<td>FSSW</td>
<td>Friction Stir Spot Welding</td>
</tr>
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<td>FSW</td>
<td>Friction Stir Welding</td>
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<tr>
<td>GA</td>
<td>Genetic Algorithm</td>
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<tr>
<td>GeNF</td>
<td>Geesthacht Neutron Facility</td>
</tr>
<tr>
<td>GKSS</td>
<td>Gesellschaft für Kernenergieverwertung in Schiffbau und Schifffahrt, now HZG</td>
</tr>
<tr>
<td>GmbH</td>
<td>Gesellschaft mit beschränkter Haftung</td>
</tr>
<tr>
<td>GoF</td>
<td>Goodness of Fit</td>
</tr>
<tr>
<td>GOM mbH</td>
<td>Gesellschaft für Optische Messtechnik</td>
</tr>
<tr>
<td>GUI</td>
<td>Graphical User Interface</td>
</tr>
<tr>
<td>HAZ</td>
<td>Heat Affected Zone</td>
</tr>
<tr>
<td>HBM</td>
<td>Hottinger Baldwin Messtechnik</td>
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<tr>
<td>HDT</td>
<td>Hole Drilling Technique</td>
</tr>
<tr>
<td>HSM</td>
<td>High Speed Machining</td>
</tr>
<tr>
<td>HZG</td>
<td>Helmholtz-Zentrum Geesthacht Centre for Materials and Coastal Research, formerly known as GKSS</td>
</tr>
<tr>
<td>IDMEC</td>
<td>Instituto de Engenharia Mecânica</td>
</tr>
<tr>
<td>iHDT</td>
<td>Incremental hole drilling technique</td>
</tr>
<tr>
<td>IMAGINE</td>
<td>Innovative Fertigungsmethoden für integrale Leichtbaustrukturen aus neuen Werkstoffen mit unterschiedlichen Eigenspannungszuständen</td>
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<tr>
<td>INEGI</td>
<td>Instituto de Engenharia Mecânica e Gestão Industrial</td>
</tr>
<tr>
<td>ISIS</td>
<td>Intelligent Sensing for Innovative Structures, Canada research network</td>
</tr>
<tr>
<td>ISRT</td>
<td>In Situ Roller Tensioning</td>
</tr>
<tr>
<td>Abbreviation</td>
<td>Description</td>
</tr>
<tr>
<td>--------------</td>
<td>-------------</td>
</tr>
<tr>
<td>IST</td>
<td>Instituto Superior Técnico</td>
</tr>
<tr>
<td>IST</td>
<td>Instron Structural Testing Systems</td>
</tr>
<tr>
<td>KG</td>
<td>Kommanditgesellschaft</td>
</tr>
<tr>
<td>K.I.S.S.</td>
<td>Keep It Simple and Stupid</td>
</tr>
<tr>
<td>LBW</td>
<td>Laser Beam Welding</td>
</tr>
<tr>
<td>LED</td>
<td>Light Emitting Diode</td>
</tr>
<tr>
<td>LEFM</td>
<td>Linear Elastic Fracture Mechanics</td>
</tr>
<tr>
<td>LET</td>
<td>Laboratório de Ensaios Tecnológicos</td>
</tr>
<tr>
<td>LoP</td>
<td>Lack of Penetration</td>
</tr>
<tr>
<td>Ltd.</td>
<td>Limited company</td>
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<tr>
<td>MAG</td>
<td>Metal Active Gas</td>
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<tr>
<td>MCA</td>
<td>Major Component Assembly</td>
</tr>
<tr>
<td>MIG</td>
<td>Metal Inert Gas</td>
</tr>
<tr>
<td>M(T)</td>
<td>Middle Tension</td>
</tr>
<tr>
<td>MTS</td>
<td>MTS Systems Corporation</td>
</tr>
<tr>
<td>MSD</td>
<td>Multiple Site Damage</td>
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<tr>
<td>NDT</td>
<td>Non-destructive testing</td>
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<tr>
<td>NI</td>
<td>National Instruments</td>
</tr>
<tr>
<td>N.V.</td>
<td>Naamloze vennootschap</td>
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<tr>
<td>OM</td>
<td>Optical Microscopy</td>
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<tr>
<td>PCBN</td>
<td>Polycrystalline Cubic Boron Nitride</td>
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<tr>
<td>PTFE</td>
<td>Polytetrafluoroethylene, commercially available as Teflon</td>
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<tr>
<td>PWHT</td>
<td>Post Welding Heat Treatment</td>
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<td>PWRT</td>
<td>Post Weld Roller Tensioning</td>
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<td>R&amp;T</td>
<td>Research and Technology</td>
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<tr>
<td>RSM</td>
<td>Response Surface Methodology</td>
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<td>SEM</td>
<td>Scanning Electron Microscopy</td>
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<td>SIF</td>
<td>Stress Intensity Factor</td>
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<tr>
<td>SHM</td>
<td>Structural Health Monitoring</td>
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<tr>
<td>S-N</td>
<td>Stress <em>versus</em> Number of cycles, fatigue experiment</td>
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<td>SSFSW</td>
<td>Stationary Shoulder Friction Stir Welding</td>
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<td>Abbreviation</td>
<td>Description</td>
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<tr>
<td>TIG</td>
<td>Tungsten Inert Gas</td>
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<td>TMAZ</td>
<td>Thermo Mecanically Affected Zone</td>
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<tr>
<td>TWI</td>
<td>The Welding Institute</td>
</tr>
<tr>
<td>USA</td>
<td>United States of America</td>
</tr>
<tr>
<td>UT</td>
<td>Universal Time</td>
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<tr>
<td>UV</td>
<td>UltraViolet</td>
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<tr>
<td>XRD</td>
<td>X-Ray Diffraction</td>
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<td>wEDM</td>
<td>Wire Electro Discharge Machining</td>
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<tr>
<td>WMP</td>
<td>Department of Solid State Joining Processes at HZG</td>
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## Symbols

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<thead>
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<th>Symbol</th>
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<th>Unit</th>
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<tr>
<td>$\alpha$</td>
<td>thermal expansion coefficient</td>
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<tr>
<td>$\alpha_A$</td>
<td>thermal expansion coefficient of the optical fibre</td>
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<tr>
<td>$\alpha_n$</td>
<td>thermo-optic coefficient</td>
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<tr>
<td>$\delta d_{hkl}$</td>
<td>change in lattice spacing of plane hkl</td>
<td>[µm]</td>
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<tr>
<td>$\delta e_i$</td>
<td>layer thickness for step i</td>
<td>[mm]</td>
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<tr>
<td>$\Delta \lambda_B$</td>
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<td>[nm]</td>
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<tr>
<td>$\delta \theta$</td>
<td>shift in the Bragg angle</td>
<td>[°]</td>
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<tr>
<td>$\varepsilon$</td>
<td>strain</td>
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<tr>
<td>$\varepsilon_{1,2,3}$</td>
<td>strain in directions 1, 2 and 3</td>
<td></td>
</tr>
<tr>
<td>$\varepsilon_{X,Y,Z}$</td>
<td>strain in directions X, Y and Z</td>
<td></td>
</tr>
<tr>
<td>$\lambda$</td>
<td>wavelength</td>
<td>[nm]</td>
</tr>
<tr>
<td>$\Lambda$</td>
<td>pitch of the grating</td>
<td>[nm]</td>
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<td>$\lambda_B$</td>
<td>Bragg wavelength</td>
<td>[nm]</td>
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<td>$\nu$</td>
<td>Poisson’s ratio</td>
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<td>$\phi$</td>
<td>diameter</td>
<td>[mm]</td>
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<td>$\phi_0$</td>
<td>reference diameter</td>
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<tr>
<td>$\sigma_r$</td>
<td>rupture stress</td>
<td>[MPa]</td>
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<td>$\sigma_{rem}$</td>
<td>remotely applied load</td>
<td>[MPa]</td>
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<tr>
<td>$\sigma_{X,Y,Z}$</td>
<td>stress in directions X, Y and Z</td>
<td>[MPa]</td>
</tr>
<tr>
<td>$\sigma_{YS}$</td>
<td>yield stress</td>
<td>[MPa]</td>
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<tr>
<td>$\theta$</td>
<td>Bragg angle</td>
<td>[°]</td>
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<tr>
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<td>Description</td>
<td>Unit</td>
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<tr>
<td>--------</td>
<td>--------------------------------------------</td>
<td>----------</td>
</tr>
<tr>
<td>a</td>
<td>half crack length</td>
<td>[mm]</td>
</tr>
<tr>
<td>A</td>
<td>extension</td>
<td>[%]</td>
</tr>
<tr>
<td>D</td>
<td>derivative part of a servo-hydraulic controller</td>
<td>[ms]</td>
</tr>
<tr>
<td>d</td>
<td>lattice spacing</td>
<td>[nm]</td>
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<tr>
<td>d₀</td>
<td>unstrained lattice spacing</td>
<td>[nm]</td>
</tr>
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<td>d₀₀ℎ𝑘𝑙</td>
<td>unstrained lattice spacing of plane hkl</td>
<td>[µm]</td>
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<tr>
<td>dₜℎ𝑘𝑙</td>
<td>lattice spacing of plane hkl</td>
<td>[µm]</td>
</tr>
<tr>
<td>E</td>
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</tr>
<tr>
<td>I</td>
<td>moment of inertia</td>
<td>[kg m²]</td>
</tr>
<tr>
<td>I</td>
<td>integral part of a servo-hydraulic controller</td>
<td>[s⁻¹]</td>
</tr>
<tr>
<td>k</td>
<td>curvature</td>
<td>[1/mm]</td>
</tr>
<tr>
<td>Kᵢ</td>
<td>mode I stress intensity factor</td>
<td>[MPa√mm]</td>
</tr>
<tr>
<td>Kᵢᵢ</td>
<td>mode II stress intensity factor</td>
<td>[MPa√mm]</td>
</tr>
<tr>
<td>Kₛᵢᵢ</td>
<td>strain sensitivity of sensor i</td>
<td>[]</td>
</tr>
<tr>
<td>Kₜᵢ</td>
<td>stress concentration factor</td>
<td>[]</td>
</tr>
<tr>
<td>Kₜᵢᵢ</td>
<td>temperature sensitivity of sensor i</td>
<td>[]</td>
</tr>
<tr>
<td>L</td>
<td>delay part of a servo-hydraulic controller</td>
<td>[ms]</td>
</tr>
<tr>
<td>Mₖ</td>
<td>bending moment</td>
<td>[Nm]</td>
</tr>
<tr>
<td>n</td>
<td>number of terms</td>
<td>[]</td>
</tr>
<tr>
<td>N</td>
<td>number of cycles</td>
<td>[]</td>
</tr>
<tr>
<td>nₑᵢᵢ</td>
<td>effective refractive index</td>
<td>[]</td>
</tr>
<tr>
<td>P</td>
<td>proportional part of a servo-hydraulic controller</td>
<td>[dB]</td>
</tr>
<tr>
<td>pₑ</td>
<td>strain optic coefficient</td>
<td>[]</td>
</tr>
<tr>
<td>R</td>
<td>load ratio for fatigue experiments</td>
<td>[]</td>
</tr>
<tr>
<td>r</td>
<td>radius</td>
<td>[mm]</td>
</tr>
<tr>
<td>R²</td>
<td>quality of fit parameter</td>
<td>[]</td>
</tr>
<tr>
<td>S</td>
<td>stress</td>
<td>[MPa]</td>
</tr>
<tr>
<td>t</td>
<td>thickness</td>
<td>[mm]</td>
</tr>
<tr>
<td>T</td>
<td>temperature</td>
<td>[°C]</td>
</tr>
<tr>
<td>W</td>
<td>nominal width</td>
<td>[mm]</td>
</tr>
<tr>
<td>Z</td>
<td>hole depth</td>
<td>[mm]</td>
</tr>
</tbody>
</table>
Dedicated to my friends
Chapter 1

Introduction

The first chapter of this thesis presents the aims and scope of the work and provides some general information concerning the development of the thesis preparation. The structure and sequence of the different chapters shown in the thesis is explained, emphasising the connection between the different topics. In this way, a roadmap for the reader is provided. Since some of the contents shown in this thesis have already been published by the author, a list of the most relevant publications is also given.
1.1 Aims and scope

Great interest is dedicated to the use of welding in aircraft structures, aiming at moving to integral structures, with economies in part count, lead time, weight and maintenance costs. FSW is a key process currently being considered for such structures. In this context, two aspects require further studies before a broad application of this process can be expected in the civil aviation market. The damage tolerance of the resulting structures has to be thoroughly studied namely due to residual stress fields and differences in the crack arresting features. Additionally the part distortion due to the manufacturing process of such lightweight components has to be controllable to the required level of accuracy given by tight component tolerances. A multidisciplinary approach is followed to support the resolution of these problems; the research was conducted in the areas of material processing, manufacturing and damage tolerance.

1.2 Location and duration

This thesis was developed mainly at three locations in Portugal and Germany represented in Figure 1.1.

Figure 1.1: Map showing the main stays during the PhD work.
Chapter 1. Introduction

The primary location was Faculdade de Engenharia da Universidade do Porto (FEUP) in Portugal, a significant amount of experiments was also performed at Helmholtz-Zentrum Geesthacht Centre for Materials and Coastal Research (HZG), formerly known as Gesellschaft für Kernenergieverwertung in Schiffbau und Schifffahrt (GKSS) and an internship was performed at Airbus Operations GmbH, both in Germany. Additionally to these main locations, smaller tasks have also been performed together with other institutions.

The thesis preparation work started with courses at FEUP during one semester. Afterwards the thesis preparation was shared between Portugal and Germany. During this time several concepts have been developed in different topics. At the end, a significant part of the acquired knowledge was applied during an half year internship in the Research and Technology (R&T) department of Airbus Operations GmbH in the manufacturing engineering group.

1.3 Structure of the thesis

The state of the art is analysed in Chapter 2 and Chapter 3 details the characterisation of the materials used throughout the entire work. Chapter 4 is concerned with a sensor system capable of measuring strain and temperature during the Friction Stir Welding (FSW) processes, with the potential of being left in place for Structural Health Monitoring (SHM). Residual surface strain may be obtained using the presented techniques, leading to some knowledge on the initial strains present on structures after manufacturing. The state of the art FSW process is already being used in the aeronautical industry at smaller scale. Related residual stress and distortion phenomena need to be better understood for accurate damage tolerance predictions and manufacturing. A thorough study concerning the influence of the FSW clamping process on mechanical properties, residual stress and distortion may be found in Chapter 5. Integral monolithic structures may also be manufactured by High Speed Machining (HSM). Redistribution of residual stresses due to material removal may lead to part distortion and therefore to expensive rejections. Chapter 6 is concerned with such problems providing a means of predicting the resulting distortion on the basis of simple measurements before time-consuming machining operations are performed. In Chapter 7 biaxial fatigue experiments of panels manufactured by HSM and FSW representing parts of longitudinal and circumferential welds on an aircraft fuselage skin are described. A strong influence of the rolling direction on the crack growth path was found for the tested aluminium lithium alloy. In linear elastic fracture mechanic analyses, one important parameter for accurate fatigue life estimations is the Stress Intensity Factor (SIF). Chapter 8 presents advances
in the application of a method for measuring the SIF directly on thin-walled structures based on the actual strain distribution around a crack tip. Such measurements may be useful for fracture analyses performed on in-service structures. Chapter 9 briefly discusses the application of studied concepts in the aeronautical industry, based on the internship that was performed. Conclusions, general remarks and an outlook on future work are provided in Chapter 10.

1.4 Published results

This thesis contains work that has already been partially published by the author in scientific journals or on scientific conferences. Part of the results presented in this thesis may, among others, be found also in the references listed below:


Chapter 5: Distortion due to FSW V. Richter-Trummer, E. S. da Silva, M. A. N. Beltrão, A. Roos, J. F. dos Santos. Distortion in Al2198-T8 Butt Joints Welded by
Chapter 1. Introduction


In order to improve the economic viability of aluminium airframes, one approach is to reduce the ratio between material supplied to the manufacturer and the material actually flying in the structure. This ratio may be improved by state-of-the-art welding techniques which reduce the need for machining large parts, since these may also be welded with high precision and mechanical resistance.

Another approach to improve the viability of metallic airframes is to use lighter and stronger alloys. Most of these alloys are however difficult or even impossible to weld with traditional fusion welding techniques, and therefore solid state welding processes such as friction stir welding have to be developed and understood.

Due to welding, distortion and residual stresses may arise. These are impossible to eliminate completely simultaneously during and even after welding without adversely affecting the mechanical resistance. Therefore these effects have to be thoroughly studied.

The present chapter seeks to provide a comprehensive description of the state of the art for the topics of interest.
Chapter 2. State of the art

2.1 Aluminium alloys

Sakata showed in 1982 that reducing the density of aluminium alloys by only 7 to 10% could provide more cost-effective structural weight reductions than composite materials at that time, due to the high production costs for composite structures [2]. Even if integral composite structures are nowadays starting to be applied in certified civil aircrafts, metallic structures and parts will remain present for decades to come. Aluminium alloys still have significant advantages in relation to aeronautical composites among others in the areas of cost, manufacturing and impact resistance.

The Aluminium Association, Inc. is the trade association for producers of primary aluminium, recyclers and semi-fabricated aluminium products, as well as suppliers to the industry [3]. This association has defined various series of aluminium alloys, based on their chemical constitution. Depending on the supplier and association, different designation schemes may be followed, but due to its wide acceptance its definitions were used throughout the present text.

Wrought aluminium is identified with a four digit number, where the first digit identifies the main alloying elements (1 = almost pure aluminium; 2 = Copper; 3 = Manganese; 4 = Silicon; 5 = Magnesium; 6 = Magnesium & Silicon; 7 = Zinc; 8 = others), the second single digit, if different from 0, indicates a modification of the specific alloy, and the third and fourth digits are arbitrary numbers given to identify a specific alloy in the series. The designation is then followed by a dash, a letter identifying the type of heat treatment (F - as fabricated; O - annealed; H - strain hardened; W and T - solution heat treated) and a 1 to 4 digit number identifying the specific temper [4-6].

The -T6 temper applied to the 6xxx series aluminium alloys means that the alloy was solution heat treated and afterwards artificial aged. The material, either directly after quenching in the -W state or already naturally aged to the -T4 temper, is subjected to a thermal cycle specific to each alloy. The alloy is normally kept at temperatures in the order of 180°C for about two hours, resulting in an increased strength but also a lower elongation. When the time of the artificial ageing process is too long or the temperature too high, then over-ageing may occur, reducing the material properties again [6, 7].

The -T73 temper is also an artificial ageing process mostly applied to the 7xxx series aluminium alloy. In this case the alloy was solution heat treated ad overaged or stabilised. Since this alloy naturally ages almost indefinitely, artificial ageing is used to stabilise its condition [6, 7]. The second digit is related to subsequent stress relieving by stretching in this case.
The -T851 artificial ageing procedure is for example applied to the AA2198 aluminium lithium alloy for obtaining the highest mechanical characteristics. This temper procedure specifies that the alloy was solution heat treated, cold worked and afterwards artificially aged. The additional digits are related to the stress relieving procedures applied after artificial ageing [6].

Generally during welding processes the temperatures are high enough for changing the temper of the welded alloys. This means that an ageing procedure would have to be performed after welding in order to get the highest mechanical properties. This is however not always practical due to the limited size of ageing ovens and the involved time. It is therefore recommended to choose welding parameters and alloys for important joints which reach high enough mechanical properties in their as welded condition.

Table 2.1 shows the calculated densities of different aluminium alloys, as defined by The Aluminium Association [8]. These properties should be kept in mind when choosing a material for the aeronautical sector, where weight is also a very important factor.

<table>
<thead>
<tr>
<th>designation</th>
<th>density $[\frac{kg}{m^3} \times 10^3]$</th>
<th>$\sigma_{YS} [MPa]$</th>
<th>E [GPa]</th>
</tr>
</thead>
<tbody>
<tr>
<td>AA7075</td>
<td>2.81</td>
<td>435</td>
<td>72</td>
</tr>
<tr>
<td>AA6082</td>
<td>2.70</td>
<td>310</td>
<td>70</td>
</tr>
<tr>
<td>AA2198</td>
<td>2.69</td>
<td>436</td>
<td>71-77</td>
</tr>
</tbody>
</table>

### 2.1.1 Aluminium-Magnesium-Silicon (specifically AA6082)

The main components of the series 6000 alloys are magnesium and silicon which precipitate in the form of $Mg_2Si$ inside a $\alpha$-phase aluminium matrix. There is often an iron corrector such as manganese or chromium; occasionally small amounts of copper or zinc to improve the strength without substantial loss of corrosion resistance; boron in conductors to remove titanium and vanadium; zirconium or titanium to control the grain size. Lead and bismuth are sometimes added to improve machinability, but they are less effective than in magnesium-free alloys.

It should be noted that different alloys within this series may have similar compositions in some cases. The mechanical properties of the commercial alloys depend on content of Mg, Si, Cu and other alloying elements, treatment conditions and exact heat treatment. Fully hardened alloys for example show some tendency to inter-granular fractures in tension testing, but manganese additions reduce this tendency. Silicon precipitates, as platelets, may be responsible for this brittleness. The structure of these alloys is
relatively simple, being the main constituent $Mg_2Si$ in an aluminium matrix, which in the heat treated condition is in solution creating the possibility of age hardening after artificial ageing. If sufficient copper and silicon are present, it may be replaced at least partly by $Cu_2Mg_8Si_6Al_5$, which will produce some hardening also with natural ageing. Series 6xxx aluminium can be precipitation hardened, but not to the high strengths that the 2xxx and 7xxx series can reach [11].

The aluminium alloy AA6082 was registered with The Aluminium Association by the European Aluminium Association in 1972 [8]. The composition limits defined may be seen in Table 2.2.

<table>
<thead>
<tr>
<th>Table 2.2: Chemical composition of AA6082 [8].</th>
</tr>
</thead>
<tbody>
<tr>
<td>Si  Fe Cu Mn Mg Cr Zn Ti Ag Li Zr Al</td>
</tr>
<tr>
<td>min 0.70 0.40 0.60 rem.</td>
</tr>
<tr>
<td>max 1.30 0.50 0.10 1.00 1.20 0.20 0.10 rem.</td>
</tr>
</tbody>
</table>

The 6000 series of aluminium alloys is well known for its good mechanical properties while maintaining a high weldability even with fusion welding techniques. Furthermore these alloys are readily available, and are therefore commonly found in the literature [12–14].

### 2.1.2 Aluminium-Zinc (specifically AA7075)

The series 7000 aluminium alloys use zinc as their main alloying element and may therefore be precipitation hardened to very high strengths.

The aluminium alloy AA7075 is a very high strength alloy, developed at the early times of the commercial jet aviation history which is still used in modern aircrafts. It was registered with The Aluminium Association by the United States of America (USA) in 1954 [8]. The defined composition limits may be seen in Table 2.3.

<table>
<thead>
<tr>
<th>Table 2.3: Chemical composition of AA7075 [8].</th>
</tr>
</thead>
<tbody>
<tr>
<td>Si  Fe Cu Mn Mg Cr Zn Ti Ag Li Zr Al</td>
</tr>
<tr>
<td>min  1.20 2.10 0.18 5.10 rem.</td>
</tr>
<tr>
<td>max  0.40 0.50 2.00 0.30 2.90 0.28 6.10 0.20</td>
</tr>
</tbody>
</table>

The alloy AA7075 ages naturally for a very long period of time, over several years. Only after artificial ageing to the T6 or T7 conditions an equilibrium may be achieved. This
means that mechanical properties of naturally aged alloys should be accompanied by
the natural ageing time [7].

Some publications concerning FSW of this alloy are available [15–18] as well as a related
review [7]. Colegrove et al. welded 6.35 and 16mm thick plates using different tools, and
found that the temperature during welding was near the solidus temperature [16]. 12.5
and 6.35mm thick plates welded by FSW were analysed by Hatamleh et al. [15, 19]. The
effect of age hardening of this alloy on mechanical properties after welding was studied
by Nelson et al. [18]. He revealed that due to the welding temperatures, parts of the
joint were overaged and did therefore not recover the full strength over time. Generally,
this alloy is considered hard to weld due to its elevated hardness, but the FSW process
is capable of joining this alloy.

Information regarding the influence of thermal and mechanical treatment of AA7075 on
residual stresses is given by Mordfin [20]. According to this author, the highest ther-
mal treatment temperature to the state T73 for this alloy is 163°C. During quenching,
residual stresses arise, but at this temperature, the stresses are reduced by about 40% to
a magnitude of around 100MPa for stretched plates in the range of 0.3 to 1.4%. Simulations showed that stretching to about 2% leads to the highest stress relieve, but
according to Boyer and Boivin [20], after 4 to 6% of stretching, significant residual
compressive stresses should be expected on the plate surface.

2.1.3 Aluminium-Lithium (specifically AA2198)

Together with the application of new design concepts, such as the use of integral mono-
lithic structures in airframes [21], the development of lighter and stronger aluminium
alloys is a key way to allow metallic structures to compete with the modern polymeric
airframes which are still very expensive.

In aerospace applications, weight is an important factor for material selection. Since for
each weight % of any of the known alloying elements for aluminium alloys, lithium gives
the greatest reduction in density and increase in stiffness [22, 23], this alloy is of high
interest for the aeronautical sector, see Figure 2.1.

Up to its solubility limit of 4.2% [23], each weight % Li reduces the aluminium alloy
density by approximately 3% and increases its elasticity modulus by 5% [24].

Small amounts of lithium allow the precipitation strengthening of aluminium though
the formation of a homogeneous distribution of coherent and spherical $\delta'(Al_3Li)$ during
heat treatment. Precipitation strengthening is achieved by ageing after solution heat
treatment [25].
The development of aluminium-lithium alloys began in the 1970s, being the first commercially available alloys the series 8090, 2090 and 2091 in the mid-1980s. These alloys had a high lithium weight percentage, but problems with their toughness. The alloy AA2198 is a third generation aluminium lithium alloy, derivate of the alloy AA2098. The goal of this new alloy was to optimise its toughness for aeronautical applications [24].

Third generation Al-Li alloys were initially developed for military and space applications. More damage tolerant alloys such as AA2198 were developed for the civil aviation market which seeks for an optimised combination between high specific strength and excellent damage tolerance [26].

In relation to prior generations of Al-Li alloys, the lithium percentage was reduced which lowered its gains in elastic modulus and density, but the higher Cu concentration makes up for these losses contributing to a higher strength potential, thermal stability and toughness. Especially in the T8 condition a good damage tolerance may be found [26].

As shown by [27] the AA2XXX series of alloys has the highest fatigue strength of all alloys, but is simultaneously one of the most difficult to weld alloy series. The difficult weldability of some aluminium-lithium alloys was discussed by Kostrivis and Lippold in 1999 [28]. These authors state that while these alloys are generally weldable by different fusion welding processes, the joint efficiency may be even below 50% for some of them when no solution heat treatment and ageing is performed after welding. Chen and Chaturvedi showed poor fatigue performance of fusion welded AA2195 even after Post Welding Heat Treatment (PWHT) [29]. Even if the PWHT increased static performance, the achieved yield strength was still only 59% of the base alloy. The fatigue life could
not be improved by the PWHT due to the presence of micro-cracks [29]. The FSW technique discussed later on will introduce a significant improvement in this area.

The aluminium alloy AA2198 was registered with The Aluminium Association (AA) by the USA in 2005 [8]. The composition limits may be seen in Table 2.4.

<table>
<thead>
<tr>
<th>Si</th>
<th>Fe</th>
<th>Cu</th>
<th>Mn</th>
<th>Mg</th>
<th>Cr</th>
<th>Zn</th>
<th>Ti</th>
<th>Ag</th>
<th>Li</th>
<th>Zr</th>
<th>Al</th>
</tr>
</thead>
<tbody>
<tr>
<td>min</td>
<td>2.90</td>
<td>0.25</td>
<td>0.10</td>
<td>0.8</td>
<td>0.04</td>
<td>rem.</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>max</td>
<td>0.08</td>
<td>0.10</td>
<td>3.50</td>
<td>0.50</td>
<td>0.80</td>
<td>0.05</td>
<td>0.35</td>
<td>0.10</td>
<td>0.50</td>
<td>1.1</td>
<td>0.18</td>
</tr>
</tbody>
</table>

### 2.2 Friction Stir Welding

Since the AA2198 alloy is considered very difficult to weld by fusion welding processes, FSW is being regarded as a possible solution, but only few publications exist which relate the new aluminium alloy AA2198 with the modern welding process FSW.

Buffa et al. studied lap-joints of 3.2mm thick AA2198-T4 sheets using different tools and welding parameters [30]. The developed model is able to predict most field variables and may therefore be used in these specific conditions to help in the process design.

The alloy AA2198 is characterised by a relatively large anisotropy in mechanical and microstructural properties between the rolling and the transversal directions. Cavaliere et al. studied the influence of this anisotropy on tensile and fatigue properties of 4mm thick AA2198-T851 FSW butt-joints. A higher fatigue life was found for base material loaded longitudinally to the rolling direction, after welding, this difference was not noticeable anymore, probably due to the microstructural changes during FSW [10, 31].

FSW was developed and patented by the The Welding Institute (TWI) in 1991 [32] as a novel solid-state joining process with the ability to weld high strength aluminium alloys difficult or even impossible to weld with fusion techniques, and even multi-material joints [12]. The process was initially introduced for aluminium alloys, but is now also being studied for steels and other metals such as cooper or titanium. It can operate in all welding positions since no fusion occurs. This property is however limited by the robots used; mostly parallel structure robots have to be used due to their higher stiffness, and these are less flexible than their serial structure counterparts. No manual welding can be performed using this method so far. The bobbin tool alternatives being studied [33, 34] relax the need for rigid robots.
No filler wire is used for this type of welding which means that only base material will exist in the final result since the pin is non-consumable - autogenous weld. This may signify that this process has a better ability of retaining the original material properties than fusion welding techniques using filler wire.

In the FSW process a probe (often also called pin) stirs the material to be joined and the rotational friction softens the metal up short of its solidus temperature, which assists in the mixing of both parts. The shoulder helps in the heating of the metal, since it contacts with the surface of the weld. The geometry of this shoulder, \( i.e. \), its diameter and form of the base does influence the final quality of the weld. A concave shoulder for example leads to better surfaces than a flat shoulder. Figure 2.2 shows a scheme of the welding process.

![Figure 2.2: Scheme of the FSW process.](image)

FSW requires a backing plate which prevents the plasticised material from being expelled on the bottom side of the weld when a vertical downforce is applied to the tool. The backing plate required for this process does also influence the welding, since it dictates the heat evacuation through the bottom of the plate, and influences the material properties and their distribution in this way.

The side of the welding where the tangent tool velocity is the same direction as the welding direction is called the advancing side, the other one is called retreating side [35].

Rigid clamping has to be used to prevent both plate halves from separating during the welding process. High reaction forces may be expected, since the probe penetrates the
metal. Bobbin tool welding is being studied as an alternative, reducing the need for backing bar and rigid clamping mechanisms.

Normally dedicated manufacturing equipment is used in this process, since a high rigidity is required along with force control. It is however also possible to use traditional machining devices, e.g. milling machines, that were altered for this new process, as in Instituto Superior Técnico (IST) or in the workshop of DEMec-INEGI (FEUP).

FSW has the ability to join different thicknesses, taylor welded blanks [36], and has other advantages like a low defect and distortion probability. Therefore it is already in use in the aerospace [37] and automotive industries for joining high strength aluminium alloys, providing clean joints and consistently high bond strengths.

As for the welding speed, according to TWI, FSW equals Metal Inert Gas (MIG) in butt-welding 6 mm thick aluminium. FSW is slower than MIG in thinner materials and faster than MIG in thicker materials [38]. The quality of the welding depends on a great variety of factors like the pin angle, rotating and advancing speeds. The influence of these parameters is currently being studied.

Defects inherent to this process are the keyholes that are created when the probe leaves the work piece at the end of the welding line. These keyholes have to be filled after the welding is done. Various methods were developed for this purpose.

Among others, FSW was applied successfully in the aerospace [37], shipbuilding [39], automotive [36] and railways industries [40].

### 2.2.1 Metallurgical phenomena in FSW

Fundamental knowledge of metallurgical features due to FSW is essential for process development. Due to inherent experimental difficulties in this area however, not much information is available. Most studies are limited to the analysis of macrographs taken after a weld. Some studies aim at following the path of marker material inserted during welding and further studies use diffraction based techniques to understand the metallurgical phenomena occurring during FSW. Only some generally accepted properties are reported regarding these phenomena.

Regarding the region around the pin, dynamic recrystallisation was found to occur during this process [35] due to the relatively high temperatures in the stir zone and the intense plastic strain introduced by the rotating tool.
Intense material flow was also found to occur in the nugget zone due to FSW. Depending on the tool geometry, material flow was found to be almost parallel to the rotating direction of the probe during Friction Stir Spot Welding (FSSW) of 1mm thick aluminium AA6061 specimens [41]. In the same FSSW it was found that since the temperature exceeded the solvus temperature in the stir zone, metastable precipitates are completely dissolved. Due to the rapid heating and cooling of this welding process, the supersaturated solid solution formed by complete dissolution of the precipitates is generally kept at room temperature [41]. This may explain precipitation free microstructures in the nugget zone of friction stir welded joints.

Furthermore microstructural features such as the precipitate density and morphology were found to correlate with the temperature profiles which occur during FSW [42]. Localized frictional heat during the FSW process was shown to produce significant microstructural changes which led to local variations in mechanical properties of the joint [43].

2.2.2 Clamping

Clamping systems hold components for the purpose of welding them together. In order to join them successfully, each component must be positioned and held securely in place during the weld process. This clamping process should additionally be repeatable in order to guarantee a constant quality.

When designing new optimised clamping systems for specific applications, crucial information regards the actual forces required to hold the parts to be joined in place. The clamping process and its implications in machining processes are relatively well understood [44]. Even the best clamping locations can be calculated adequately in order to guarantee machinability [45], but no information is given by Jeng et al. in relation to their effect on plate distortion.

For welding processes, less information is available. Liu et al. studied the clamping force experimentally for a laser beam weld [46]. One load cell was used perpendicular to the welding line on the plates’ plane. While this leads to some interesting information, no information is given regarding the force perpendicular to the plane of the plate. According to Liu et al., for a fusion welding process model [47], the welding distortion decreases with increasing restraining force. This should hold true for the solid state welding process as well.

To the author’s knowledge, almost no information is available regarding clamping forces used in FSW. Arbegastr, for example, demonstrated experimentally that the clamping
location may influence, among others, the process forces [34] but not enough detail is given regarding the actual forces used.

According to Smith et al. [48], FSW requires significant forces that fixturing must support, which significantly increases its cost. No details are however provided. This may be related to the complexity of measuring clamping forces in different directions independently. Clamping force adjustment by air pressure is already used industrially [49] for traditional welding processes, but also in these cases no information is given on the influence of this welding force on the weld quality. Most of these clamping devices are oversized due to the lack of better information regarding FSW clamping requirements.

A different approach was taken for example by the Edison Welding Institute (EWI) [50]. Local clamping was studied in order to be able to clamp complex structures in controlled conditions. Improved part fit-up, weld quality and appearance may be achieved in this case. Axial load control was used to maintain a constant clamping force. No information is given regarding the clamping in the other directions.

This general lack of information regarding clamping for FSW and its influence on the joint properties is a concern of the present thesis. Knowing the actual required clamping forces may lead to better clamping systems, but also to more accurate process simulations.

2.2.3 Temperature evolution during welding

All welding processes generate heat to provide a means of joining different materials. Fusion welding processes need temperatures above the fusion temperature of the materials to be joined. Solid state welding processes need temperatures in the range of 70 to 90% of the fusion temperature [7].

In the FSW heat is generated by the friction between the tool and the work-piece, namely the pin via plastic deformation and the shoulder via rotational friction [35].

For hard to weld materials with low thermal conductivity like titanium, special techniques are being tried out. Among them the Stationary Shoulder Friction Stir Welding (SSFSW) technique used by the TWI [51] reduces the frictional heat in the shoulder by stopping its rotation, and concentrates therefore the heat in the welding region near the pin.

It should be noted that different materials require different approaches. For example for welding titanium, heat resistant Polycrystalline Cubic Boron Nitride (PCBN) tools may be used, and it is difficult to introduce enough heat in the nugget zone. Welding of
cooper leads to problems with the very high heat flux away from the nugget zone due to the high thermal conductivity.

Accurate temperature acquisition, especially near the tool is very difficult, since the process destroys almost every measuring device located in the area near the tool. Therefore most of the results do not include, or simply extrapolate, information in the welding region.

Thermocouples are the most commonly found temperature acquisition devices in this kind of experiments. Type-K thermocouples for example are cheap, measure temperatures up to over 1000°C and provide accuracy in the range of 0.1°C.

Fibre Bragg Grating (FBG) sensors may be used for the same purpose, but these sensors are still being developed and are therefore not so widespread. More information regarding the use of these sensors may be found in Chapter 4 or [52].

Thermographic imaging provides interesting details about the time resolved surface temperature distribution on friction stir welded plates. Anyway, due to the rigid clamping, which hides big parts of the plate, and different reflective properties of the aluminium plate and the welded zone, this technique may lead to difficulties.

Another temperature measurement method, is through in situ, time resolved neutron diffraction [53]. Using this technique, it was possible for the first time to simultaneously measure temperature and thermal stresses inside a FSW joint. For this experiment, a portable FSW machine was placed in the radiation chamber and welded at a very low speed of 0.42mm/s, leading to a measuring time of over 30 minutes, which was enough for data acquisition. The neutron scattering volume was fixed in relation to the tool, making use of the quasi-steady state of this joining process for extending the neutron collection time. Both the quasi-steady state condition and the repeatability of the process itself where successfully demonstrated. While this information is not complete, because of the low welding speed and the use of the quasi-steady state condition, no neutron source is yet powerful enough to perform direct in situ experiments with high temporal and spatial gradients. Difficulties arise in this measuring process, since lattice spacing is influenced by both, the elastic stress due to strain and the plastic strain due to temperature changes among others. Advanced calculations including the access to temperature dependent elastic modulus of the studied material are required to separate both phenomena. For AA6061-T6, temperatures of about 360°C are reported below the FSW tool.

During frictions stir spot welding, temperature was found to reach more than 430°C in the stir zone [41].
2.2.4 Strain evolution during welding

The strain evolution during different welding processes may be of high interest, since its knowledge permits to have information regarding the residual stresses which were introduced during the welding process. With most measurement methods, only surface information may be captured, but this is enough for thin plate weldings. For example electric or optical strain gauges may be used for strain determination. In this case the measurement devices have to consider the initial state of the plate as the reference. During welding, the surface strains evolve and finally stabilise at some value after cooling down. This residual strain value may be related to residual stresses present in this plate. After residual stress measurements on a few specimens, the residual stresses may be predicted by knowing only the surface strain which is easy to measure.

The measurement technique should be well chosen, since all methods have specific advantages and disadvantages. For example resistive strain gauges measure mechanically induced strain, since the thermal expansion of the specimen is compensated by the expansion of the strain gauge grid [54]. FBG sensors are not self temperature compensated, which means that the sensors will measure the mechanically applied strain and the thermal expansion of the surface they are bonded to. Digital Image Correlation (DIC) based systems may measure strain on complex surfaces and show its distribution. These systems measure the total strain similarly to FBG sensors, but unfortunately the expected noise is relatively high, since an ideal setup is very difficult to achieve.

In situ stress evolution was also measured by Neutron diffraction. The evolution of the stresses during welding shows a compressive stage below the tool, changing to tensile stresses as the weld cools down. About 100mm behind the tool the stresses created in the process already reached the final residual stresses measured at room temperature [53].

Measurement of strain during welding processes is complex due to the difficult access to the surface, the very high temperature and strain gradients and the generally high temperature of the surface.

2.2.5 Process forces during FSW

It was verified that the welding parameters and the tool profile influence the process forces acting on the tool. This information is important, among others, for the design of new tools and the selection of the tool material.

Different forces act on the tool during the welding process, including forces in all three spatial directions, bending moments and torque.
Process forces were studied by Arbegast [34] and Hattingh et al. [55] among others [56, 57]. The second author used a multi-axial transducer installed in the tool holder for this task.

Fleming et al. showed that gaps may be detected by using information about the force acting during the welding process [58]. Such findings pave the way for a better automation of this welding process making it more attractive for the industry, where quality concerns have to be seen together with cost. This work showed that in lap joints a sudden drop in force is a sign for a gap in the material to be welded, even when on the surface of the weld no defects are visible. Therefore the information acquired by the force transducers has to be automatically transformed so that only relevant information is passed on to the process controlling computer. This process is not production ready, but it demonstrates the interest in knowing process forces and possible uses for them in industrial scale applications for in situ quality control.

It is often verified that the stiffness of the FSW machines is not high enough to avoid the displacement of the pin from the centre line due to process forces. As shown by Balasubramanian et al. [56], the material being displaced from the advancing side to the retreating side pushes the pin into the region between the advancing side and the trailing edge. Knowing this kind of forces makes it possible to detect, or at least suspect of, defects when the force pattern changes.

As shown by [59], process forces used as a feedback signal may even be used for tracking the position of the tool in relation to the weld seem. Therefore it is however necessary to extract relevant data from the force measuring devices and introduce it into the control algorithm of the FSW machine. In this case, a general regression neural network was used to relate changing process forces with the offset from the welding line and at least in the test runs, interesting results where obtained showing that this seam tracking works in principle.

According to [56], the magnitude and location of the resultant welding force can lead to a better understanding of the material flow patterns. One result obtained by [56] is that the magnitude of the resultant forces seems to be reduced when welding parameters are chosen which introduce more heat into the material. This may be due to the softening of the material surrounding the pin.

Kohn et al. explored the option of heating the workpiece by laser simultaneously during FSW [60]. This laser assisted FSW approach allows to reduce welding induced forces due to the softening of the material achieved by the laser heating process.
2.2.6 Material flow during FSW

Flow visualisation studies using Synchrotron X-Ray Diffraction (XRD) sources and numerical analysis [61] were performed in order to understand the material flow behaviour during this welding process. Computer micro-tomography results showed that at least part of the material being stirred flows around the tool 1.5 times [62].

During traversing of the material, the pin contacts with new material in front of it along its leading edge. This material is moved from the advancing side to the retreating side via the leading edge and finishes at the trailing edge of the pin due to the tool rotation. The material then flows into the void left in the pins wake and consolidation is achieved by the vertical forging force applied by the shoulder [56].

Computational Fluid Dynamics (CFD) was used to simulate the material flow around the tool with varying results, since the material flow stress is temperature and strain rate sensitive, and the distribution of heat is itself governed by the deformation and temperature fields [63]. Very complex models are needed in order to be able to retrieve useful information and mostly the relevant material data are not available.

Heurtier et al. used automated Electron Backscattered Diffraction Scan (EBSD) in order to characterise the grain structure of a aluminium joint of AA2024 [64]. Differences were determined between the advancing and retreating side. While on the first a sharp boundary was found between the nugget and the Thermo Mecanically Affected Zone (TMAZ), on the retreating side no clear boundary was found [64].

The formation of the well known onion ring pattern on FSW macro-sections was studied by Kumar and Kailas [65]. These authors define two combined kinds of material flow, the pin and the shoulder driven material flows. During the welding process, the pin driven flow transfers material layer by layer, and on the other hand the shoulder driven material is transferred by bulk. The interaction of these material movements leads to different etching bands on macro-sections, usually described as onion pattern [65].

2.2.7 Flaws and Defects in FSW

Some authors distinguish between flaws and defects. A flaw may be considered a feature that one would prefer not to have in a weld. Only if a certain flaw compromises the structural integrity it is designated as a defect [63].

Due to the low total heat input, FSW does not yield any crack formation and porosity right after the welding as may happen in fusion welding techniques [66]. Anyway, typical flaws include voids, root flaws and plate thinning [63].
Different defect detection techniques are available, and the need for improved inspection techniques leads to new approaches as shown by various authors. Lévesque et al. studied the laser ultrasonic technique to detect defects successfully for defect sizes coinciding with the condition of reduced mechanical properties [67]. Santos et al. studied non-destructive techniques based on eddy currents capable of detecting defects in friction stir welded samples in some cases [68].

2.2.8 Process parameters and tool geometry

Process parameters and tool geometry of FSW are not well understood yet. Most of the joints are based on empirical knowledge, since no general model or database is available. Smaller databases on the other side are mostly not released to public domain in order to maintain competitive advantages. Therefore the present section shows some of the available information regarding process parameters, with the goal of giving some limits where parameter studies should be performed for thin aluminium sheets. Furthermore, this general information helps in determining the capabilities of custom FSW machines.

Rajamanickam studied the effect of process parameters on thermal history and mechanical properties for a AA2014 FSW joint by experimental and numerical means. He showed that the temperature below the tool was strongly dependent on the tool rotation speed, but the mechanical properties where more strongly dependent on the welding speed [43].

Hatting et al. showed the influence of tool geometry on welding forces and mechanical properties of the joint [55]. For this purpose a tool instrumented with a multi-axial transducer was used. For the various tools, a torque range between 40 and 70Nm was determined for a 6mm thick plate of AA5083 [55]. In this paper a detailed study of the influence of the tool geometry was performed. This is very important due to the high influence, not yet well understood, of this geometry on the joint.

Elangovan et al. studied to influence of the tool geometry and rotational speed on joint properties of 6mm thick AA6061 sheets [69]. The rotational speed was shown to lead to best mechanical properties at 1200rpm, while above and below this value slightly lower properties were obtained. Also square and triangular tool geometries led to better properties than feature less cylindrical shapes, which is probably due to the better material mixture obtained with tools with more features. The rotational speed strongly influences the energy input in the joint, which has to be balanced between energy input through the pin and the shoulder, and energy output through the air, clamping and backing of the weld.
Deqing et al. determined experimentally that a relation between shoulder and tool diameter of 3:1 leads to the best weld quality measured in terms of tensile strength [70].

For 3mm thick plates of AA5083 and AA6082, depending on the advancing and rotational speed torque values between 30 and 80Nm were measured [13]. A higher advancing and rotational speed leads to higher torque, being the rotational speed the more influential parameter.

Shi et al. studied the influence of process parameters on plate distortion of 3mm thick AA6013 joints. Higher rotational speeds were found to produce a more pronounced distortion [71].

Bitondo et al. determined welding parameters for AA2198 sheets with a thickness of 3.2mm [72]. Depending on the optimisation goal, the rotational speed was determined to be between 500 and 672rpm and the advancing speed between 150 and 300mm/min [72].

Adamowski et al. studied different parameters for AA6082 joints with a thickness of 5mm [73]. Different joint properties were found as a function of the process parameters, being the tensile properties proportional to the advancing and rotational speeds.

To a limited extent, models may be used for calculation of the energy introduced into a weld by FSW [66, 74–77]. This allows to predict temperatures and other phenomena while virtually changing welding parameters. Due to the complex nature of the welding process however, no complete model has been developed yet.

### 2.3 Residual Stress

FSW creates residual stresses of not negligible magnitude in the work-piece. While the thermal gradients created in this process are lower than in most traditional welding techniques, residual stresses are also influenced by the mechanical stirring process, and therefore accurate predictions of the residual stress based on process parameters is difficult. Attempts of modelling this welding process are being made, although with reduced success due to the high complexity of the welding process [78, 79]. Both the thermal effects and mechanical effects have to be considered in detail for a successful model. Furthermore, the missing material properties at different temperatures turn modelling into a difficult task.

Threadgill et al. summarised the characteristic magnitude and profiles of longitudinal residual stresses across a FSW seam, and concluded that most alloys have a peak residual stress near 200MPa under the shoulder edge and lower residual stresses in the pin area.
leading to a 'M' shape [63]. Usually a tendency for getting slightly higher tensile stresses on the advancing side than on the retreating side is observed.

This 'M' shape arises, since ahead of the tool compressive stresses caused by the expanding hot material lead to plastic straining due to the low compressive yield strength, while behind the tool, tensile stresses begin to appear longitudinally as the weld material cools down. However the weld line stress development is limited due to the low tensile yield strength because of the high welding temperatures. The magnitude of the longitudinal stresses may be similar to the yield strength of the welded material [80]. Therefore local plastic straining happens at the weld line, which then results in the formation of a dip in the developing residual stresses. This leads to the typical M-shaped residual stress profile [81].

As stated in [82] residual stresses are often dealt with in a very conservative fashion in damage tolerance considerations. More information regarding the residual stress state and its evolution in fatigue life is therefore required for achieving an optimised lightweight design using modern alloys and recent joining processes efficiently.

The access to synchrotron XRD allows measuring the same specimen before and after fatigues tests with good accuracy and in a relatively short time, allowing tests which consider the specimen-to-specimen variation of residual stresses.

During fatigue loading, it was verified that fatigue loading accentuates the compressive and tensile peak stresses while retaining the general form of the residual stress field in a cyclically hardening alloy. Measurements showed a tendency of stresses becoming more positive with a higher number of fatigue cycles [82].

Although the influence of residual stress fields in fatigue crack propagation is well understood, its characterisation is a difficult task particularly as concerns the actual measurement of the residual stress state. Most frequently available data is incomplete, e.g. surface or near-surface measurements in the case of XRD [83] or sectioning [84] techniques. This limitation may be overcome by using high energy diffraction techniques such as neutron or Synchrotron XRD diffraction, that may be able to provide non-destructively information on the residual stress state along an area of interest. However, access to facilities providing these high energy diffraction techniques is scarce worldwide. There is therefore a large interest in affordable techniques that might provide the complete characterisation of residual stress fields along a surface of interest such as in the case of the crack plane in Compact Tension (C(T)) specimens. The contour technique, an emerging residual stress measurement procedure originally proposed by Prime [85], may provide that characterisation. So far, published welding applications of the contour
technique concern mainly residual stresses parallel to the joint in butt welded plates. No application to welded C(T) specimens is available.

Data on residual stress fields in welded C(T) specimens include early results obtained by the sectioning technique [86], the cut-compliance method [87], the XRD diffraction technique [88–90] and more recently by the neutron diffraction technique [91]. Rading [91], dealing specifically with the through the thickness variation provides data only for two situations: near surface and mid thickness; this is most likely related to the beam time necessary for the acquisition of each data point. Other measurement results are available for multi-pass welds but with plate geometries different from C(T) specimens. Using centre cracked plates, Paradowska et al. showed that neutron diffraction based though-the-thickness measurements are possible, and that well established fitness-for-purpose approaches, such as BS7910 and R6, may lead to comparable but overestimated data in certain cases [92].

2.3.1 Measurement methods

Residual stress measurement techniques may be divided into three main groups. Some review articles are available related to measurement of residual stress [78, 93–95] and a good and short description of different destructive and non-destructive measurement techniques is given by Radaj [78] in the context of welding residual stresses.

Destructive techniques are the first group, were a full relaxation of the residual stresses to be measured is achieved by material removal. In these techniques, the deformation due to the full relaxation of residual stresses is measured, and stresses are calculated. No distinction can be made between the types of measured stresses. This type of measurement may in principle be applied to any kind of material.

The second group of measurement techniques englobes the semi-invasive techniques. Here only a small portion of material is removed and the resulting relaxation is measured. In general, these techniques allow further usage of the measured part, although in some cases the removed material has to be added again, for example through fill welding.

Non-destructive diffraction techniques are part of the third group of measurement techniques and basically measure crystal lattice spacing. Therefore the strain-free lattice spacing has to be known for the sample, which is normally not trivial in welded or rolled plates. This is similar to measuring relaxation, but the stress free state is measured on a different sample as the stressed state.

Due to the different assumptions necessary for the residual stress measurement techniques, it is recommended to use various methods based on distinct assumptions in
order to get a high degree of confidence in the results. Systematic errors can be minimised in this way. For example the diffraction based synchrotron XRD technique can be used together with the relaxation based contour method, but comparing two diffraction based techniques could lead to erroneous results.

Figure 2.3 summarises information regarding the different residual stress measurement techniques [53, 83, 85, 96–100]. A comparison regarding the measuring depth of each of the methods is made. It should however be noted that penetration depth depends on the specific problem conditions and the numbers given are only indicative.

Some methods for residual stress measurement available in Portugal are shaded in Figure 2.3. Measurements have been made with all three types of measurement techniques and in depths ranging from 0 to 32mm. While this information may not be complete, it is based on published data and own experience.

Given the great variety of measurement techniques with different capabilities, advantages and disadvantages, the most important part for measuring residual stress, is to decide which technique to use and how to apply it. Detailed knowledge regarding each measurement technique is required, and the stress to be measured should be well defined. It is not feasible to measure the complete residual stress field in all directions on all points in a specimen. Therefore some preliminary knowledge regarding the residual stress present in a specimen leads to a more efficient way to work. This task should always be performed together with experienced residual stress experts.

Figure 2.3: Penetration capability measured from the specimen surface for each method [53, 83, 85, 96–100]. The shaded parts of the Figure represent measurement methods available in Portugal.
2.3.1.1 Laboratory X-Ray Diffraction

X-Ray Diffraction is a non-destructive residual stress measurement technique where strain is measured in the crystal lattice. The number of grains measured depends on both the material and the measurement setup. Laboratory X-ray sources have wavelengths of typically around 1.5\,Å, are limited to penetration depths below 100\,µm, and therefore only produce data for surface stresses. Detailed information may be found in technical guides [83, 101].

XRD should preferably be used for surface related measurements such as the analysis of effects of surface treatments such as laser or shot peening for introduction of surface compressive residual stresses [15, 99].

XRD measure diffraction peaks of suitable intensity of fine grained crystalline materials [102]. Unfortunately this measurement technique may therefore not provide good results for all materials, such as some large grain aluminium alloys.

Depth profiling may be performed in certain cases by successively removing layers of material for example by electro polishing [83]. In this case, the method may not be considered non-destructive.

2.3.1.2 Contour method

The contour method for residual stress determination has been developed by Prime in 2001 [85]. It consists in the measurement and analysis of a cut-surface in the plane where residual stress is to be determined. The relaxation due to the cut is measured and then used to infer the residual stresses that were present at the plane of interest before the cut was made. Only stresses perpendicular to the cut surface can be determined by this method. The following procedure is used to obtain an accurate model of the residual stresses by the contour method.

- The specimen is cut along the plane where residual stresses are to be determined. This relaxes all stresses present in the direction perpendicular to the cut surface.
- The cut surface is measured with high resolution and precision equipment.
- The measured deformation is prepared for application in a numerical model.
- The prepared deformation data is applied to a fully elastic Finite Element Method (FEM) model of the specimen and the residual stresses present before the cut was made are calculated.
This method requires that the cut occurs along a virtually flat plane [85]. Therefore
the clamping during the cutting process plays a fundamental role. Also after the mea-
surement, the average of the displacements of both plate halves should be taken as the
deformation to be applied to the FEM model, since in this way errors may be reduced
or even eliminated.

Except for numerical problems due to the large FEM models, this method should be
applicable to the measurement of through-the-thickness residual stresses in complex
structures. This process may be used to analyse a great variety of geometries without
high investments in specialised equipment. It is however a destructive method and
therefore not applicable in all situations.

The measurement procedure used requires physical measuring of a few thousand points.
It is therefore a time-consuming method. To reduce this problem, Prime developed
a laser surface measurement alternative [103]. The precision and accuracy of the best
current touch probe based Coordinate Measuring Machines (CMMs) is around 3µm. For
small specimens, this may be a problem, since deformation and not strain is measured,
and therefore the measured displacement depends on the dimensions of the specimen and
not only on the stresses. For welded plates above 150mm, deformations of over 0.5mm
were found [80], which means that the precision limit of the machine introduces noise,
but without significant effect on the results. With a laser measurement this precision
could be augmented, and the measured grid could become finer, without increasing the
measuring time.

Preparing the data for application in a FEM model is not trivial. It has to be guaranteed
that at least the first order derivative of the deformation surface is continuous. Therefore
the following steps have to be followed.

- The mean value of each data-point should be taken between both measured plate
  halves, interpolating them onto the same grid, in order to guarantee that every
  single points error is reduced.
- The measured data has to be smoothed and fitted to continuous functions with
  continuous first derivatives. In this way, the interpolation of the node values to
  the Gauss points does not create errors and the stresses are correctly passed from
  one element to the next.
- The smoothed data has to be aligned to a virtual and horizontal cutting plane,
  since there is no guarantee that the measured cut-surface was in an overall hori-
  zontal position while measured by CMM. This procedure guarantees the moment
  equilibrium of the surface residual stresses.
The mean value of the deformation has to be subtracted in order to guarantee that in the linear model the tensile and compressive stresses have similar integral values along the cut surface. This guarantees that the plate is in equilibrium, and no reaction forces result from the deformation of the cut surface.

This calculation method is based on the elastic superposition principle, which means that if plasticity is involved, the calculated results would contain an error. If residual stresses are high enough to cause yielding during the cutting process, errors may arise from this measuring method. As shown by Shin [104], in the contour method the plasticity induced error depends upon the location of the constraints used during the cutting process. The restraining process should therefore guarantee that the plane of cut does not move during the machining process. If this is done, the introduced errors are negligible. As long as the determined peak value of residual stress is well below the yield stress determined by a tensile test, it may be assumed, according to [104], that the error is indeed negligible.

This method is applicable to the studied problems related to welding residual stresses. Near surface results should however be analysed with care, since these are calculated in the extrapolated area of the performed measurements. Full field, but only one stress directional stresses can be determined with a high accuracy.

An example application of this method with a detailed explanation of all necessary steps may be found in Appendix B.

2.3.1.3 Synchrotron X-Ray Diffraction

As in the other diffraction based techniques, a crystal lattice is employed as an internal 3D strain gauge. A change \( \delta d_{hkl} \) in the spacing of a set of lattice planes \( hkl \) of formerly unstrained spacing \( d_{hkl} \) produces a corresponding shift \( \delta \theta \) in the position of the Bragg reflection originally at an angle \( \theta \) that is related to the lattice strain \( \varepsilon \) by the Bragg equation (see Equation 2.1) [98].

\[
\varepsilon = \frac{\delta d_{hkl}}{d_{hkl}} = -\cos \theta \delta \theta
\]  

(2.1)

Synchrotron sources provide a range of radiation of the order of a million times as intense as laboratory X-ray sources. As the attenuation of X-ray beams varies with the atomic number and approximately as the cube of the wavelength, high-energy X-rays can penetrate significantly in aluminium. For example, 0.3Å X-rays can penetrate about 100 times deeper than those of wavelength around 1.5Å. This favourable combination
of intensity and penetration can make synchrotron X-rays superior to neutrons for investigating thin samples of light element materials such as aluminium alloys. In these samples it is possible to make measurements in both transmission and reflection [98].

Since synchrotron radiation has a typical wavelength of about 0.3\AA, a gauge volume of about 0.1mm$^3$ can be obtained due to its linear resolution of about 20\(\mu\)m. Penetration depth in aluminium may exceed 20mm, and the acquisition time is normally short for each data point, allowing the measurement of a great amount of points in a typical experiment [82].

Synchrotron X-ray strain scanning was used to determine the residual stress distribution in FSW of the aluminium alloy type AA7108 in the T79 condition. In that case, such as recommended for face-centred cubic crystal structures, measurements were made using the 311 reflection and a wavelength of about 0.35\AA. [98]

For measuring thin aluminium specimens, the beam energy is around 40keV and an X-ray wavelength of approximately 0.3\AA should be used. Such hard X-rays are around 100 times more penetrating than typical laboratory X-ray sources with a wavelength of around 1.5\AA, but give lower scattering angles of typically 10° to 30°. Strain sensitivity and angular dispersion are lower at these angles, and peak angular shifts under typical values of residual stress are of the order of 0.01°. Therefore an accuracy higher than 0.001° is required for the measurements of the angular peak position. The ratio of the gauge length to width may be as high as 10:1 in these cases, which means that it has to be taken into account when combining synchrotron strain scanning data to obtain stresses [105].

### 2.3.1.4 Neutron diffraction

Neutron diffraction based techniques lead to full three dimensional information with penetration depths up to 100mm in aluminium. The data acquisition is however fairly slow. The neutron scattering volume is typically over 1mm$^3$ due to the linear resolution around 500\(\mu\)m. Since the method is non-destructive, it may be used on a wide variety of samples, but detector size constrains limit the overall dimension of measurable samples. Furthermore this method is not field applicable [106].

The neutron diffraction technique determines the elastic lattice strain from a small shift in the measured angular position of a Bragg diffraction peak. Such a shift results from the straining of polycrystalline materials [107]. If strains are determined in three independent directions at each point, the stresses may be calculated by Hooke’s law. Neutron diffraction is therefore is a volume averaged measurement of inter-planar lattice
spacing (d-spacing) in a crystalline material based on the Bragg’s law, from which the apparent lattice strains can be determined [53].

The lattice spacing perpendicular to the lattice planes in a scattering volume may be determined by Bragg’s law 2.2 where \( \lambda \) is the wavelength of the neutron, \( d_{hkl} \) represents the lattice spacing of atomic planes characterised by the Miller indices \{hkl\} [108].

\[
\lambda = 2d_{hkl} \sin \theta_{hkl}
\]  
(2.2)

\( 2\theta_{hkl} \) is the scattering angle of the peak being measured. A variation \( \Delta d \) of the lattice distance due to internal strain results in a shift of the reflex \( \Delta \theta \) which is measurable. The angle position of the reflex is determined by fitting it to an adequate function.

Type III stresses lead to a symmetric widening of the reflex, not to a shift in its position. The influence of type II stresses may be minimised by the correct choice of the lattice reflex. This is why the stresses obtained using neutron diffraction correspond in a good approximation to the macroscopic type I stresses. The (311)-reflex is recommended for measurements in materials with a face-centered cubic crystal structure, since this normally leads to realistic type I stresses [109, 110].

The strain \( \varepsilon \) perpendicular to the reflecting plane of the crystal structure may be calculated by equation 2.3.

\[
\varepsilon = \frac{d - d_0}{d_0} = -\frac{\theta - \theta_0}{\tan \theta_0}
\]  
(2.3)

The variables \( d_0 \) and \( \theta_0 \) are the inter-planar spacing and diffraction angle of the strain free grid. Strain measurements have to be performed in at least three consecutive vertical directions in order to determine the strain in one direction. In this context one limitation of this measurement method should be mentioned. It is necessary to measure a strain free sample in order to be able to compare the distance \( d \) with \( d_0 \). Woo et al. reviewed neutron diffraction residual stress measurements related to FSW. Showing several examples the author concludes among others that, due to the severe microstructural changes observed in a friction stir weld, the d-spacing is changed turning measurements more challenging. These changes may account for measurement errors in the range of 50% of the stress to be determined. Therefore the author recommends that several small size cubes are cut from each location of a weld in order to determine the corresponding \( d_0 \) value [108].

The measuring volume may be defined by cadmium apertures in the entering and diffracted beam. Both cubic and longitudinal measuring volumes may be selected. The
longitudinal volumes generally lead to higher intensities and therefore to shorter measuring times. This should however only be done if the strain along the longitudinal direction of this volume is approximately constant. A scattering angle of \( \pi/2 \) is often used for neutron diffraction measurements since it leads to cubical gauge volumes [108].

Hooke’s law shown in Equation 2.4 for the main directions \( i = X, Y \) or \( Z \) is normally used to convert measured elastic strain to principal residual stresses. It should be mentioned, that in the case of thin plates \( \sigma_Z \) is assumed to be negligible, reducing it to equation 2.5.

\[
\sigma_i = \frac{E_{hkl}}{1 - \nu_{hkl}} \left[ \varepsilon_{ii} + \frac{\nu_{hkl}}{1 - 2\nu_{hkl}} (\varepsilon_{XX} + \varepsilon_{YY} + \varepsilon_{ZZ}) \right] \quad (2.4)
\]

\[
\sigma_{X,Y} = \frac{E}{1 - \nu^2} [\varepsilon_{X,Y} + \nu \varepsilon_{Y,X}] \quad (2.5)
\]

In equation 2.4, conventional diffraction elastic constants have to be used for the elasticity modulus \( E \) and the Poisson value \( \nu \). \( E_{hkl} \) and \( \nu_{hkl} \) represent the plane specific analogues of Young’s modulus and Poisson’s ratio respectively [108]. These depend on the reflex used for measuring the strains [111]. The conventional diffraction elastic constants may be calculated by the Kröner model using single crystal constants. However, if the elastic anisotropy is small, the macroscopic values for these constants may be used [81]. The value for \( d_0 \) has to be defined carefully since it depends, among others, on the number of atoms present in the matrix and the chemical composition of the material. In a plane stress situation, \( d_0 \) may be calculated according to Equation 2.6, since \( \sigma_z = 0 \).

\[
d_0 = \frac{1 - \nu}{1 + \nu} d_z + \frac{\nu}{1 + \nu} (d_x + d_y) \quad (2.6)
\]

The equipment used for the present work was the ARES-2 diffractometer at Geesthacht Neutron Facility (GeNF) at GKSS, [112]. A monochromatic neutron beam with a wavelength of 0.164nm was available. The monochromator takeoff angle could be varied continuously between 57.3° and 120.3°. It was equipped with a double focusing monochromator with perfect silicon (311) crystals. This monochromator had a fixed vertical curvature (three crystal slabs mounted one above the other) and a variable horizontal curvature.

2.3.1.5 Incremental hole drilling technique (iHDT)

The iHDT is based on the standard hole drilling technique, but due to the special data reduction applied, it is able to determine information on stress variations through the thickness of a component.
The American Society for Testing and Materials (ASTM) standard E837 [113] and the Measurements Group Technical Note TN-503-6 [114] should be followed for best results. The Hole Drilling Technique (HDT) is one of the most widely used techniques for measuring residual stress, since it is relatively simple, inexpensive, quick and versatile.

Its functioning principle is based around the introduction of a hole into a body with residual stresses which relaxes the stresses at that location. The HDT for residual stress measurements was first proposed by Mathar in 1934 [115]. Presently, this method is a widely accepted technique for measuring residual stresses. It is a semi-destructive technique where a tolerable small volume of material is removed. The basic hole drilling procedure, involves drilling a small hole into the surface of a component at the centre of a special strain gauge rosette and measuring the relieved strains.

The method is very versatile and can be performed either in the laboratory or the field, on different materials, and on components in a wide range of sizes and shapes. The hole is typically 0.8mm to 4.8mm in both diameter and depth. Nevertheless, achieving accurate results is not trivial; a meticulous measurement practice and the adequate choice of data analysis method are crucial for obtaining good results. A very well written good practice guide for the application of this technique is also available [96].

The theoretical background for the hole drilling method was first developed on the basis of a small hole drilled completely through a thin, wide, flat plate subjected to uniform plane stress. Such a configuration is far from typical applications since ordinary components requiring residual stress analysis may be of any size or shape, and are rarely thin or flat.

The aim of the hole drilling method is the evaluation of the in-plane residual stresses that can be assumed uniform with depth either from the surface of a thick specimen, or through the thickness of a thin specimen. The ASTM standard E837 [113] refers to these cases. However, in many practical cases, the residual stresses are not uniform with depth. In such cases, the assumption of uniform stress with depth may give a misleading solution.

A blind hole produces a very complex local stress state which implies the use of empirical techniques for calculation of the residual stresses from the measured strains. The incremental hole drilling technique, which involves carrying out the drilling in a series of small steps, improves the versatility of the method and enables stress profiles and gradients to be measured.

The data-reduction relationships, Equation 2.7, are applicable to the blind-hole when appropriate blind-hole coefficients are known. Compared to the through-hole procedure,
blind-hole analysis involves one additional independent variable; namely, the dimensionless hole depth.

\[ \varepsilon_r = (\bar{A} + \bar{B}\cos 2\beta) \sigma_{\text{max}} + (\bar{A} - \bar{B}\cos 2\beta) \sigma_{\text{min}} \quad (2.7) \]

A generalized functional form of the coefficients can be expressed as shown in Equations 2.8 and 2.9.

\[ \bar{A} = f_A (E, \nu, r, Z/\phi) \quad (2.8) \]
\[ \bar{B} = f_B (E, \nu, r, Z/\phi) \quad (2.9) \]

In these equations, \( r \) is the ratio between the hole diameter and an arbitrary radius from hole centre. For any given initial state of residual stress and a fixed hole diameter the relieved strains generally increase (at a decreasing rate) as the hole depth increases. Therefore, in order to maximise the strain signals, the hole is normally drilled to a depth corresponding to at least \( Z/\phi = 0.4 \) (ratio of the hole depth to the mean diameter of the strain gauge circle). For any given set of material properties, elastic modulus \( E \) and Poisson’s ratio \( \nu \), the coefficients \( \bar{A} \) and \( \bar{B} \) are simple geometric functions, and thus constant for all geometrically similar cases.

Whether the residual stress analysis application involves through hole or blind-hole drilling, the coefficients \( \bar{A} \) and \( \bar{B} \) must be determined to calculate the stresses from the relieved strains. In the case of the through hole, the coefficients \( \bar{A} \) and \( \bar{B} \) can be accurate values obtained by analytical calculation. Nevertheless, the needed coefficients for either through-hole or blind-hole analysis can always be determined by experimental calibration. A technical note by the Measurements Group describes how to obtain the coefficients [114].

A fundamental test should be made to check whether the residual stresses are uniform through the whole depth. This test indicates which type of analysis should be performed, for uniform stress data or for non-uniform stress data. As recommended in ASTM E837 [113], it is always preferable to drill the hole in small increments of depth, recording the observed strains and measured hole depth at each increment. This procedure allows the judgment whether the residual stress is essentially uniform with depth, thus validating the use of the standard full-depth coefficients and for calculating the stress magnitudes.

The incremental data, consisting of relieved strain versus hole depth, can be used to detect if a non-uniform stress distribution is present. The Standard outlines the graphical procedure, for determining stress uniformity based on combination strains. The sums and differences of the measured strain data \( \varepsilon_3 + \varepsilon_1 \) and \( \varepsilon_3 - \varepsilon_1 \) or \( \varepsilon_3 + \varepsilon_1 - 2\varepsilon_2 \) should
be calculated for each depth increment [113]. The data should be expressed as fractions of their values when the hole depth equals 0.4 times the mean diameter of the strain gage circle. The data points indicate the percentage values of the specified strains and the curves show the limits of the two largest computed combination strains. The graph should yield data points very close to the curves presented in the corresponding ASTM standard. Data points that deviate by more than 3% from the curves presented in the standard indicate either substantial stress non-uniformity through the material thickness, or strain measurement errors.

At least five techniques for analysing residual strain data are available in the literature: Uniform Stress, Equivalent Uniform Stress, Power Series, incremental strain method and the Integral method. According to Grant and Lord [116], developments of the integral method were introduced by Niku-Lari et al. in 1985 [117] among others, where finite element calculations were used for calibration. In the integral method, the contributions of the total measured strain relaxations of the stresses at all depths are considered simultaneously [118]. This provides a separate evaluation of residual stress within each depth increment. Previous studies showed that the Integral method usually leads to the best results for non-uniform stress fields, in particular those where the stress varies rapidly with depth, for example as shown by Grant and Lord [116].

According to Grant [96] the integral method is able to decode relaxed strains that relate to highly non-uniform residual stress distributions. The same author states that for residual stresses near the yield stress, the integral method overestimates the true residual stresses within the calculated increment, but in the following increment, the calculated stress is an underestimation. Oscillations of this nature should be easily detected and such residual stress results have to be treated with extreme caution. The ASTM standard E837 [113] for the application of the HDT to uniform and non-uniform stress along the thickness also recommends this data reduction technique for similar cases.

Usually this method is not recommended for residual stresses above 60% of the yield stress of the bulk material, since the stress concentration around the drilled hole can lead to local yielding. In this case, the results would be affected by the measurement method itself. In specific conditions however, for example the case of shot peened surfaces, Nobre et al. demonstrated the capability of measuring higher stresses by comparing to XRD results [99]. This is most likely due to the increased performance of the treated surface layer.

For the present case this method is of high interest, since it allows to easily measure the surface and sub-surface residual stresses present in rolled or welded plates. The drawback is that only punctual information is gathered, and therefore various holes have
to be drilled for a more complete picture of the residual stress distribution. Furthermore, due to the available equipment, only the upper mm can be measured using this method.

### 2.3.1.6 Layer removal method

The layer removal method for residual stress measurement is a fully destructive method based on the measurement of the distortion of a specimen due to the removal of layers of material with residual stresses. Schajer recommends the use of the layer removal technique when stresses are known to vary through the thickness, but are uniform parallel to the surface [119].

Radaj describes the application of the layer removal method related to uniaxial and biaxial residual stress states. The residual stress present at the bottom of each layer before layer removal may be related to the distortion by differentiation and integration of the deflection along the thickness. The equations describing this relation are derived from the elementary beam bending theory. By extending the layer removal method from beam to plate problems, biaxial and homogeneous depth-dependent residual stress states, as expected in the present rolled sheet metal, may be determined [78]. The same author recommends to remove layers only to about mid-thickness, among others since the risk of exceeding the yield limit increases with the clamping being applied on lower thicknesses.

Ekmekçi et al. applied the layer removal method to a thin parallelepiped test specimen with the dimensions $70 \times 10 \times 2 \text{mm}^3$. Layers were removed discretely by electrochemical polishing, since this process is known to introduce little to no additional stress, and measurements were performed after each removal increment [120]. In this case, only the surface stresses were measured.

A similar approach was followed by Rao et al. in order to measure residual stresses in 40mm thick steel specimens [121]. Layers with a thickness of 0.2mm were removed in each step with a surface grinder, almost up to the middle of the specimen. Paterson and White applied the layer removal technique for measuring residual stress in a polymer with a Young’s modulus which is not constant along the thickness [122].

The layer removal method for residual stress measurement was first introduced for a biaxial stress state by Treuting and Read [123]. This method is applicable when the measured stresses are constant in the plane of the sheet metal and vary across the thickness, for example as it happens in rolled sheets. The equations derived by Treuting and Read are based on the fact that without external loads, the residual stresses are self-equilibrating. Removing a layer of the material leaves this through the thickness.
distribution unbalanced and after releasing the restraints, the specimen will deform in order to restore the internal equilibrium [123].

Greying et al. modified the layer removal technique for application to thermal spray coatings. Since the layer removal technique is based on the assumption that strain change is linear, the strain on the surface opposite to the removed layer may be related to the residual stress due to the necessary force and moment equilibrium. This is based on the assumption of homogeneous material properties through the thickness, which is not true for coated materials [124].

Schajer and Prime discuss reverse solutions for residual stress measurement techniques, including the layer removal method [125]. It is mentioned that the remaining material after the last increment should be similar in thickness to each increment, in order to make a realistic estimation. In the present case, it should be sufficient to remove material up to mid-thickness due to the assumed symmetry of the residual stresses in relation to the mid plane.

Hospers et al. shows the layer removal technique applied to rolled sheet metal, which is similar to the application intended [126]. Chemical etching is used for removing each layer and the curvature of the sheet is measured. The author derived equation 6.1 for determining the residual stress incrementally [126], which will be used in the present work.

\[
\sigma_{xi} = \frac{E}{12(1-\nu^2)} \left( t_i^3 k_i - t_{i-1}^3 k_{i-1} \right) - \frac{1}{2} \Delta e_i \sum_{n=1}^{i-1} \sigma_{xn} \Delta e_n \left( t_i + \frac{\Delta e_i}{2} \right) \]  

(2.10)

As described by Hospers et al., a layer of thickness \( \Delta e_i \) is removed in step \( i \). Afterwards, both the thickness \( t_i \) and the curvature \( k_i = \frac{1}{r_i} \) is measured. The values with index \( i - 1 \) are already known from the preceding step. These authors found a good qualitative agreement when compared to X-ray diffraction measurements, but he also found quantitative differences at greater depths. Results are provided for 6 mm thick sheet metal.

### 2.3.1.7 Other methods

Several other methods exist for residual stress measurement, which were not used in the present work for different reasons. Some of the techniques as described below.

The fully destructive sectioning residual stress measurement technique allows to measure average stresses on thin specimens perpendicular to a cutting line. A high number
of strain gauges is bonded to the specimen surface near the cutting line. When the sectioning is performed, the strain gauges will record the relaxation of the stresses initially present in the area of interest. Even if the process was used at an earlier time, one of the first publications explicitly mentioning the process was done in 1997 for a 7mm thick plate [84]. This method, albeit fast and simple, does only provide an approximate solution due to the experimental difficulties to make a perfect cut very near of small strain gauges. It is not recommended to apply this method on thick specimens, since through the thickness variations of the residual stresses may not be adequately captured. Furthermore no variation across the thickness may be measured.

An approach based on monitoring small velocity changes in laser generated surface skimming longitudinal wave permitted the approximate measurement of residual stresses in a non-destructive manner [67]. This method was used before by Lingfeng et al. for measuring surface and near-surface residual stresses in a fusion welded mild steel plate with reasonable results [127]. This method requires a high quality surface on the specimen in order to reflect enough of the laser beam, which may be inconvenient for most practical applications.

Magnetic effect methods may be used on ferromagnetic materials only and are therefore not useful for the present work [128, 129].

The ring core method is similar to the hole drilling technique, but the material is removed around a special strain rosette instead of in its centre. Due to the necessary special equipment, this method was not used for the present work [130].

A more advanced hole drilling technique was developed since 1992 in the University of Bristol, substantially funded by works related to the nuclear industry - the deep hole drilling technique. The company VEQTER uses this method commercially for the characterisation of through the thickness residual stresses in thick material. Through the thickness bi-axial residual stress distributions may be measured. For aluminium alloys, the nominal accuracy is 10 MPa, while the process is still relatively fast to apply. The Deep Hole drilling (DHD) method is a semi-invasive technique, since the holes have to be re-filled in certain cases depending on the application.

The method is based on four steps described as by VEQTER [97]. First reference bushes are attached and a small diameter reference hole is gun drilled through the component and bushes. Secondly the diameter, $\phi_0$, of the reference hole is measured through the entire thickness of the component and reference bushes using an air-probe. This is the diameter of the hole when residual stresses are present in the workpiece. In the third step a cylinder of material containing the reference hole along its axis is cut from the component by Electro Discharge Machining (EDM), relieving the residual stresses.
In the last step the diameter, \( \phi \), of the reference hole is measured again through the entire thickness of the cylinder and reference bushes. In this stage no residual stresses influence the measured diameters. The difference between both measured diameters allows calculating the residual stresses that were present before drilling.

Even having numerous advantages, only material thicknesses between 10 and 750mm may be measured, and therefore this technique is not applicable in the present work.

As shown by Surech in 1998, it is possible to determine surface residual stresses and residual plastic strains using instrumented sharp indentation as long as a equal-biaxial state of residual stress uniform over a depth (beneath the indented surface) which is at least several times larger than the indentation contact diameter is present [131]. This method is applicable directly to thin films such as the ones found in microelectronics and microelectromechanical systems, or coatings and surface treatments for structural applications [132]. According to Surech, this method is based on the following steps [131]. First the reference for the subsequent analysis has to be established. A quantitative instrumented indentation in the material in a residual stress free region is performed. The load versus indenter penetration depth curve during both loading and unloading is obtained for the unstressed material. Afterwards the same instrumented indentation on a surface containing equal-biaxial residual stresses is performed. From the relative compliance values of the obtained curves, the sign of the residual stress may be readily identified. Next identification of the indentation contact areas for both cases is required. The average pressure by calculating the indentation area and observing the indentation curve may then be obtained. The measured and calculated values allow to determine the residual stress.

This technique was for example successfully used to determine residual stresses due to ion implantation [133]. It was found that this method compared well to numerical models.

Later this technique was improved in order to be able to provide results for material properties and residual stresses in uni-axial stress fields [134].

These methods are based on the indentation curves only. A more complete method was proposed for example by Bocciarelli in 2007. Additionally to the indentation curve, the shape of the plastically deformed imprint is also considered. According to this author, while the indentation curve is almost insensitive to the direction of pre-existing bi-dimensional stress states, the mapped imprint reflects all features of residual stresses, including bi-axiality [135]. Therefore the information provided by the curve is joined with the information provided by the imprint and an inverse analysis is performed leading to encouraging results when compared to other experimental data.
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The instrumented indentation technique was applied to the evaluation of welding residual stress in A335 P12 steel welds. Comparison with the results of conventional methods showed that the indentation tests could be effectively and easily used for quantitative and nondestructive assessment of welding residual stresses in industrial structures [136].

The indentation method may also be used in dissimilar joints such as at the interface between two materials. As shown by Bolzon, in the production process of CuCFC joints, due to thermal expansion mismatch, residuals stresses may arise, which can be successfully determined by this process [137]. The methodology proposed by Bolzon et al. is based on the combination of experiments, their computer simulation and inverse analysis.

In general, this residual stress measuring method does not provide, however, any significant advantage for the pretended work, since it requires an inverse analysis, prone to errors and uncertainties, and is only sensitive to surface residual stresses. Even if additional data can be acquired in this form with significant work involved, the obtained information would probably not be enough to justify the investment.

2.4 Strain and Distortion

The knowledge of temperature and strain variations during welding processes allows for a better process control and optimisation, possibly also leading to lower distortions and residual stresses. Strain and temperature gradients may be reduced and the heat source may be optimised in order to reduce energy needs for a sound weld.

Once the residual stresses exceed a critical value, they may lead to bending or buckling and angular, or even rotational distortion and component shrinkage [138].

For different fusion welding techniques, Colegrove et al. found that the distortion was directly linked to the heat input of the welding process [139]. Since FSW has a relatively low heat input, this should also lead to lower distortions. Anyway, the FSW process is more complex to simulate due to the plastic deformation in the joint.

Distortions may be related to the residual stress distribution. Dong, for example, notes that welding residual stresses may be of the bending and of the self equilibrating type. While bending type means that tensile residual stresses are found on one surface and compressive residual stresses are found on the other, the self equilibrating type has the same kind of stresses on both surfaces, while in mid-thickness the opposite residual stresses lead to equilibrium. Mostly both types of residual stress distribution may be created by welding processes. The bending part is related more to the restraining
conditions of the plate, while the self equilibrating part is mainly defined by welding parameters and procedures [140].

Thin plate distortion and buckling may be, among others, a result of the plate rolling process, machining, cutting, thermal treatment and welding process [141].

Distortion which appears after cutting or machining rolled plates is mostly unpredictable, since there is a lack of knowledge of the level and distribution of residual stress in rolled plates. Even if this distortion is reduced or eliminated by a cold levelling process, the residual stresses are still not known [141].

Cutting process and sequence can also influence distortion and residual stress, due to heat input concentration. The welding process itself could be optimized for distortion, if the plate could be welded from one side, than turned over and welded from the other side. Distortion may be equalised in this way, problems with residual stresses however persist [141].

Generally, if distortion is reduced by application of force, such as bar straighteners or mechanical tensioning during or after welding, the residual stress generation and redistribution should be kept in mind [141].

In some welding processes, it is possible to reduce the heat input by not welding the whole line, or by reducing the penetration, but this solution is not acceptable in aeronautical structures subjected to fatigue, since stress concentration points would be artificially created in the structure.

For plates with thicknesses of 10mm or more, heat straightening may be considered [141].

Thin plates, stiffened by multiple welded stiffeners, are a good way to achieve high structural strength with low weight and minimum cost. The welding process however, introduces significant distortions and residual stresses, even when low heat-input processes such as FSW or Laser Beam Welding (LBW) are used. By using FEM simulations, it was found that greater distortion and residual stresses are generated when the stiffeners are closely spaced [142].

Zain-ul-abdein et al. developed a model capable of predicting the distortion due to LBW to an acceptable degree of accuracy [143, 144].

It was found that while the panel length influences the longitudinal distortion, the transverse distortion is not affected. The distortion value per unit length is not influenced by the total plate length. An increase in welding rotation speed in the same experiments
showed an increase in distortion in both directions and also a changed distortion shape [71].

The source of distortion is the unrecovered plastic strain due to the localised heat history which creates local plastic strain [71].

The shape of the distortion is found to be the opposite of traditional arc welding techniques, being the magnitude significantly lower which is credited to the lower global heat input in the FSW process [71].

2.4.1 Measurement methods

2.4.1.1 Post-mortem: Coordinate measuring machine

A traditional and well implemented technique to measure distortion in components is the use of touch probe based coordinate measuring machines. This kind of equipment is based on a robot arm which continuously measures the exact location of its arm, and a ruby touch probe with an appropriate geometry which senses every contact with the workpiece. This way, every time contact is made with the component, the machine records the corresponding coordinates.

Different machines exist, starting from almost manual machines for controlling single points to fully automated measuring robots which continuously verify the quality of components.

According to Bosch [145], coordinate measuring machines were first introduced in the 1950s by the company Ferranti, Limited company (Ltd.) of Dalkeith, Scotland as a companion product to their growing family of numerical controlled machine tools in order to be able to accurately measure in shorter time periods.

2.4.1.2 Post-mortem: Stereovision based systems

One possible method for distortion measurement is a stereo vision based system. An example for such an equipment is the Pontos system provided by Gesellschaft für Optische Messtechnik (GOM mbH). It is a non-contact measurement process, although markers have to be applied to the objects surface. Each marker is recognised by each of the cameras, and by knowing the exact spatial position and orientation of the cameras, it is possible to calculate the position of each marker. The position information of a set of markers may later be used for distortion measurements.
2.4.1.3 In situ: Digital Image Correlation

DIC is a promising new contact-less deformation measuring technique. It be used for full field, non-contact 2D and 3D measurement of deformations and strains on the surface of components [146–148]. Although the idea of analysing images and variations between images in a sequence has been used for a long time in scientific work, only recently have the computers evolved to a level where this process can be automated efficiently and at low cost.

One of the advantages of DIC is that no physical sensor has to be installed. Once the area to be measured is acquired, a variety of different sensors in different directions may be positioned. The gauge length or area may be chosen after the test, and three dimensional information is obtained for each gauge area. This kind of measurement systems is flexible, since it allows to measure almost any kind of deformation in time and space, giving access to information about strain gradients and their variations in time. Longitudinal and transverse strains with any load, such as tensile, compression, bending and torsion or a combination of different loads can be monitored [147].

The method is based on the correlation of two images acquired before and after deformation. The recorded images are analysed and compared by a special correlation technique, which allows the determination of the surface displacements with high local resolution. In order to achieve a good correlation, the method uses a speckle pattern applied to the object surface and tracks the grey value pattern in small neighbourhoods called subsets during deformation.

A disadvantage of this process however is that this process works best in laboratory under known conditions. Care has to be taken to have a clear view; fumes and similar can create noise and distorted results. The installation of such a system may be complicated when space is constrained.

A possible approach for DIC is to paint the surface to be measured with a stochastic pattern and acquire it using a stereo camera setup while it deforms. This technique is for example used by Aramis from GOM mbH.

Using this technique, events that occur during a welding process, such as the pin penetration, may be easily correlated with the measured strain results, since the events are captured in video, synchronised in time with the strain information.

In appendix G, a comparison of DIC based measurements and strain gauge based measurements is shown. For a constant temperature uniaxial tensile test recorded with only one camera, the results are nearly identical with both methods, being the advantage of the DIC based method that the distribution on the whole surface may be measured,
whereas the strain gauge only records localised strain. In the case of a welding experiment however, were two cameras were used and strong temperature variations and gradients exist, the results differ significantly, see Figure G.9. The high noise of the optical strain measurement system is also not acceptable for the proposed task.

2.4.1.4 In situ: Strain gauge

Strain gauges are the more traditional way of acquiring strain on surfaces. Their use is well documented and the results may be used for validation of novel measuring processes as long as the ambient conditions permit their usage. Standard strain gauges should not be used beyond 60°C continuous or 90°C short term working temperature [149]. Special high temperature capable strain gauges have to be used in welding applications.

In the present work, strain gauges are essentially used for validation of FBG sensor results during a welding process. Maximum temperature of use is essentially limited due to the use of cyanoacrylate based glue which has an important loss of properties for such high temperatures and does therefore not guarantee a good transmission of strain form the plates surface to the strain gauges measuring grid.

2.4.1.5 In situ: FBG sensor

A FBG is a type of distributed Bragg reflector contained in a short segment of optical fibre that reflects particular wavelengths of light and transmits all others. This is achieved by adding a periodic variation to the refractive index of the fibre core, for example by the means of a laser beam, which generates a wavelength specific dielectric mirror. A fibre Bragg grating can therefore be used as a wavelength-specific reflector. Different signals may be measured with the same fibre at the same time, since the acquisition system can separate the signals due to different base wavelengths used for reflection. This kind of reflector was first demonstrated by Hill et al. in 1978 [150].

Some of the unique advantages of FBG based fibre optic sensors are the immunity to electromagnetic and radio frequency interferences, the light weight, the capability of intermittent readings with reconnection between readings and the wavelength multiplexing capability, as described in the Intelligent Sensing for Innovative Structures (ISIS) design manual for FBG sensors [151].

Applications are frequently found in civil engineering, such as monitoring of bridges. While Barbosa et al. [152] demonstrated the use of FBG sensors for monitoring a newly built bridge, Figueiras et al. [153] applied this monitoring technique to a nineteenth century metallic bridge. Due to frequency coding of the measurements, the output does
not depend on the signal intensity. This allows very long distances for data transmission, while still retaining adequate signal strength [151]. This capability may be favourable for the final application of FBG based sensors for SHM in large aeronautical and railroad cars. As stated by Thursby [154], the knowledge of the load history of a part of a structure can provide valuable data for prediction of its lifetime.

If the load history also includes loads applied during manufacturing and not only the loads applied during the service-life, a more complete picture can be drawn about the real state of a structure, including residual stresses which may be present in the structure at the beginning of the in-service-life. For this information to be included, instrumentation has to be applied before the part is produced, as in the welding processes dealt with in the present paper. The present paper is a first step in that direction.

Data on the application of FBG sensors for monitoring welding is very scarce and concerns fusion processes only. Suarez et al. [155] presented a first attempt to monitor welding induced strains using FBG sensors, where temperature measurements were validated using thermocouples but strain results were not compared with measurements made using alternative techniques. Suarez et al. [155] presents results for the plate side opposite to the weld bead only, and the grating length is not given in that paper. Later on, Moreira et al. [156] presented further results concerning temperature monitoring using 30mm long gratings, validated using thermocouples and also thermography, on the weld bead side of the plate.

The basic working principle is for example illustrated in an Hottinger Baldwin Messtechnik (HBM) technical report [157]. Figure 2.4 shows a schematic representation of the working principle of this kind of sensor.

In a FBG sensor, the quantity to be measured causes a shift in the Bragg wavelength, $\Delta \lambda_B$.

The relative shift in the Bragg wavelength, $\frac{\Delta \lambda_B}{\lambda_B}$, due to an applied strain ($\epsilon$) and a change in temperature ($\Delta T$) is approximately given by equation 2.11.

$$\left[ \frac{\Delta \lambda_B}{\lambda_B} \right] = (1 - p_e)\epsilon + (\alpha_L + \alpha_n)\Delta T \quad (2.11)$$

where $p_e$ is the strain optic coefficient, the thermal expansion coefficient of the optical fibre is $\alpha_L$, and the thermo-optic coefficient is given by $\alpha_n$ [158].

As it can be seen in equation 2.11, eliminating the strain component by special bonding techniques, the temperature variation may be isolated from other signals as long as the shift in the Bragg wavelength is known for the fibre used. Knowing the temperature and
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the material constants, the calculation of strain is a matter of subtracting the known temperature component from the measured shift.

Some of the unique advantages of fibre optic sensors, and in the present case FBG based fibre optic sensors, are the immunity to electromagnetic and radio frequency interferences; the light weight; capability of intermittent readings with reconnection between readings and their wavelength multiplexing capability [151].

Traditional applications are mostly related to civil engineering, such as the already mentioned monitoring of bridges [159], taking advantage of the frequency coding of the measurements which allow very long distances for data transmission, in excess of 10km, still retaining over 50% of the original signal strength [151], since the output does not depend on the signal intensity. This capability, although not used in the present work, may be favourable for the final application of FBG based sensors for SHM in large aeronautical structures. The knowledge of the load history of a part of a structure can provide valuable data from which the lifetime of a structure can be predicted [154].

If the load history also includes loads applied during manufacturing and not only the loads applied during the service-life of the structure, a more complete picture can be drawn of the real solicitations of a structure, including residual stresses which may be present in the structure at the beginning of the in-service-life. For this information to be included, instrumentation has to be applied before the part is produced; in the present case the welding process is studied for this purpose.

Figure 2.4: Basic working principle of a single mode FBG sensor.
UltraViolet (UV) light at 248nm passing through a phase mask for about 30 to 40 seconds writes a sensor at a specified wavelength. Using the pulsed output of a laser together with an appropriate phase mask, the UV light changes the refractive index of the Ge doped core at precisely spaced periodical locations creating a well defined grating \[151\]. During the welding experiments the fibres are protected from UV light. This way the grating only reflects light at the inscribed central Bragg wavelength $\lambda_B$. This can be seen in Equation 2.12, where $\Lambda$ is the pitch of the grating and $n_{eff}$ is the effective refractive index.

$$\lambda_B = 2n_{eff}\Lambda$$  \hspace{1cm} (2.12) 

Due to physical changes of the sensing device, such as the application of strain or temperature, the pitch $\Lambda$ of the grating is changed, and therefore the measured wavelength is also shifted, reflecting the change in strain as can be seen in 2.11.

The shift in the Bragg wave length recorded includes the effect of the thermal expansion of the base material. Contrary to electrical strain gauges, FBG sensors do therefore also react to the thermal expansion of the specimen material.

One of the limitations of the Bragg grating technology is its cross sensitivity to temperature. Approaches to solve this problem in different conditions are mentioned by Ferraro et al. \[160\], and a solution based on a second grating free from mechanical strain was deemed appropriate for the current application. Therefore two Bragg structures were used at the same location, where the reference grating is sensitive to temperature only (unstrained) and the second one is sensitive to both strain and temperature.

In this case, it is possible to compensate for the temperature component of the second FBG sensing device. The linear equations 2.13 and 2.14 can be written.

$$\Delta \lambda_{B1} = K_{T1}\Delta T$$  \hspace{1cm} (2.13) 

$$\Delta \lambda_{B2} = K_{T2}\Delta T + K_{S2}\Delta \varepsilon$$  \hspace{1cm} (2.14) 

$K_{T1}$ and $K_{T2}$ are the temperature sensitivities of both FBG sensors, and $K_{S2}$ is the strain sensitivity of the second FBG sensor. When the two FBG structures are near, $K_{T1} = K_{T2}$ and equation 2.14 will be given by equation 2.15.

$$\Delta \lambda_{B2} - \Delta \lambda_{B1} = K_{S2}\Delta \varepsilon$$  \hspace{1cm} (2.15) 

In this case, it is possible to compensate for the temperature component of the second FBG sensing device.
For the analysis to work, some requirements have therefore to be met. Each sensing location has to have temperature and strain information, so that the temperature cross sensitivity can be compensated and additionally the thermal expansion of the analysed thin walled structure may be subtracted from the results.

The strain calculated in this way is comparable to the strain gauge results and represents the mechanical surface deformation without the thermal effect.

Even though a Bragg sensor is capable of being used as a distributed sensor, due to the available equipment, in the present case only the average strain between the first and last etched regions of the grating is measured.

Data on the application of FBG sensors for monitoring welding processes is very scarce [155, 156], being the aim of this work to enhance the knowledge in this regards.

FBG based optical strain gauges are commercially available from HBM under the designation K-OP and K-OR [161] for example. They offer a similar installation procedure to electrical strain gauges, since they are already embedded, and have the option for additional potting for protection. As an option, the connectors may be included with the fibres, and due to the protected sensing area and fibre, these optical sensors are sturdier than the ones used throughout this work.

The available Bragg wavelengths are between 1520nm and 1580nm. Their maximum elongation is ±1%, and the maximum operating temperature is 80°C. Without potting, the size of the sensors is 30±1×5±1×0.5±0.01mm³. No information is given on the available gauge lengths, but this is expected to be near 20mm. The degree of optical reflection of these sensors is around 15% and they are therefore compatible with most commercially available fibre interrogator units.

Another interesting FBG based sensor available commercially from HBM is a strain rosette - HBM K-OR. This rosette includes 3 sensors on one backing, installed at an 60° angle in relation to each other. While this rosette is interesting for general measurements, it may only be operated at a similar temperature range to the K-OP sensor. Its relatively large dimensions do not make this sensor a good selection for high strain gradient areas [157].

Since the present work is related to measuring strains and temperatures in high thermal and mechanical strain gradient areas, the commercially available sensors are too big, and do not withstand the necessary temperature. Additionally, the commercially available strain gauges are prepared for constant temperature measurements or at least with low thermal gradients, since they are too big in order to enable temperature measurements sufficiently near the strain sensing fibre for subtraction of the thermal effects.
2.4.2 Plate distortion comparison parameter

Shape distortion is difficult to adequately compare among different specimens. It is however required not only to perform a qualitative comparison, but a quantitative assessment of the shape distortion may be of high interest for comparing different specimens. In order to extract one numerical value that characterises the whole shape distortion, the minimum distance between planes that englobe all measured points and are parallel to the best fit plane through the point clouds centroid was searched. Figure 2.5 shows an example of the planes used for definition of this comparison parameter. Three planes may be seen: the first cutting through the distorted shape, which represents the best fit plane for the whole specimen, and the second and third planes parallel to the first, which intersect the distorted shape at its highest and lowest point respectively. The extracted parameter is the distance between the second and third planes.

![Figure 2.5: Out of plane distortion measured by the stereo vision based method - parallel planes.](image)

As can be seen, the planes parallel to the best fit plane through the centroid of the distorted shape passing through the minimum and maximum points of the distorted shape do not exactly represent the minimum distance between parallel planes enclosing the shape, but closely resemble this information while retaining a easy calculation procedure.

The parameter obtained in this way may be compared between similar sized plates. While this parameter is not sufficient to completely characterise a distorted shape, it provides a good means of understanding how strong the distortion is. This parameter will therefore be used throughout this work.
2.4.3 Residual stress engineering and control of distortion

Residual stresses may also have positive effects in certain applications, such as the introduction of surface compressive stresses by shot peening for reduction of fatigue crack initiation in certain locations at the price of tensile residual stresses in less susceptible areas.

Global tensioning of the welded product is often referred to as a solution for reducing tensile residual stresses in the welding region. This process is however not practical, essentially when complex shapes or big components are welded. Local roller tensioning during and after welding was investigated for mitigation of this effect [81]. If plastic compression is achieved in the normal direction by the applied roller load, it will be compensated by plastic material elongation in the rolling direction. This means that the tensile elastic strain and the longitudinal residual stresses are decreased. It was found that Post Weld Roller Tensioning (PWRT) has a strong effect on distortion for AA2199, effectively reducing the distortion levels by using a down force of 20kN. The residual tensile stresses in this case are greatly reduced using a down force of 10kN, even inverting the stress signal for down forces starting at 20kN. The In Situ Roller Tensioning (ISRT) has shown no significant effect for AA2024 in these experiments.

For good residual stress engineering and for verification of the obtained results, accurate measurements have to be available anyhow. Distortion alone on the other side may be reduced by direct observation. This observation does not provide, however, enough insight into the residual stress state of the workpiece for being able to relate both phenomena directly.

Controlling the heat sinking of welds may help to control the welding distortion. Adak and Madal built a model which is able to predict the welding distortion and the influence of heat sinking [162]. This may help to develop an adequate cooling system.

Transient thermal tensioning may also be used for welding distortion mitigation when installing stiffeners as demonstrated by the EWI [163]. This process involves using dynamic local heating at strategically located areas in relation to the weld to create thermal stresses. These stresses counteract stresses created during welding and can therefore reduce buckling distortion [163]. McPherson considers that heat straightening may be successfully used for plates thicker than 10mm for distortion mitigation [141].

Luan et al. report having used hammer treatment to reduce distortion of thin stiffened panels. This process is based on simple plastic deformation of the workpiece after welding. The same authors suggest that further experiments showed better results when appropriate cooling and a roller were used during the FSW process [164].
Xu et al. used an electromagnetic impact system for residual stress and buckling distortion reduction. When a capacitor is discharged through a coil, a transient magnetic field can induce eddy currents in metallic materials. The interaction between the magnetic field and eddy currents leads to repulsive electromagnetic forces in the metal which influence the buckling distortion [165].

2.5 Temperature

Heat in FSW is created by the tool and the shoulder. The plastic deformation in the pin region and the friction of the shoulder on the workpiece create heat, which may be controlled by the welding parameters. The heat flows into the tool, the workpiece and the surrounding. The heat flow into the workpiece is partially responsible for residual stresses and distortion and also for the overall weld quality. The tool life may be adversely affected by too much heat flowing into the tool. Depending on the workpiece material, the tool material has to be chosen accordingly in order to have a higher melting temperature.

Temperature is one of the most commonly measured effects of welding processes. Temperature may be measured by different techniques which are detailed below. The knowledge of the temperature field allows to have some information regarding the energy equilibrium in a weld. Temperature measurements along the welding line allow the verification of steady state. The temperature history of different welds may also be compared for quality assurance reasons.

Temperature is one of the most used effects for verification of numerical models due to its wide availability and relatively simple measurement.

2.5.1 Measurement methods

Different measurement methods may be used, while some of them give full field surface results, others measure the temperature at a point or the average value on a line. Temperature may be measured at the surface of an object, or up to a certain extent in the inside of a body.

2.5.1.1 Thermocouple

Thermocouples are widely accepted temperature measuring devices, due to their large temperature range, stability and low cost. Using thermocouples a large number of
points can be measured. In this work they are therefore used for validation of FBG sensor results and for temperature measurements in welding processes.

Thermocouples measure temperature on the first contact point between both wires. In this work type K thermocouples (chromel alumel) with 0.08mm diameter wire were used, which give reliable temperature results in the range of -200°C to 800°C while keeping the price-point relatively low. They may also be used with a slightly higher temperature range (-200°C to 1100°C), although attention must be paid to de-calibration and drift due to oxidation. Thermocouples of type K have a sensitivity of 40.44µV/°C.

2.5.1.2 In situ: FBG sensor

FBG sensors are sensitive, among others, to the temperature of the fibre. As long as the fibre is isolated from other influencing factors by proper installation, this kind of sensors may be used for accurate temperature measurements [156]. Due to the size of the sensor, normally longer than 2mm, the measured temperature will resemble an average temperature in the gauge area, see Chapter 4.

2.5.1.3 In situ: Neutron Diffraction

Woo et al. were able to measure the temperature inside a FSW joint, by welding in the beam-line [53]. While the separation of different phenomena which lead to changes in the measurable lattice spacing of the material is difficult, the author claims to be able to isolate the thermal strain in the measurements. These authors measured temperatures above 350°C below the tool shoulder for 6.35mm thick AA6061 sheets.

2.5.1.4 In situ: Thermography

Infrared thermography is a method were the infrared emission of a body is translated into temperature. The emissivity may be captured by a thermographic camera, allowing a complete surface observation [166]. The thermal emissivity of a body may be mapped and recorded, but no direct measurements can be performed below the surface. The thermal radiation from a body depends on its temperature and its surface emissivity. In ideal conditions, the measurement range may reach approximately -40°C to 1500°C, while keeping an accuracy as good as 1% [167].

Thermography was successfully used for welding monitoring [156, 168], although in the weld bead region results may be very difficult to interpret due to the change in emissivity
of the solidifying bead. The emissivity of materials depends strongly on the surface condition [169] and therefore may turn result interpretation into a difficult and error prone task.

2.6 Material characterisation

Material characterisation in the presented works is mostly divided into mechanical and metallographic characterisation. Also simple chemical analyses may be of interest in some cases.

2.6.1 Mechanical

When damage tolerance and joining technologies are discussed, it is impossible to avoid mechanical material characterisation experiments. These tests provide a feedback regarding the strength of a joint and the properties of damage tolerant structures. In this section, both static and cyclic experiments are presented, which may be used for basic mechanical material characterisation.

2.6.1.1 Bending test

Bending tests may be performed for various reasons. In the experiments presented in this thesis, the three point bending test was used for mechanical characterisation of FSW joints, and the four point bending test was chosen for calibration of FBG sensors. Figure 2.6 shows a comparison of the internal loads seen by a specimen when subjected to a three and four point bending test.

As can be seen in Figure 2.6, the three point bending test leads to the highest bending moment in the centre of the specimen. This property is used for detection of root defects in FSW joints. The joint is positioned in the centre of the testing setup. Due to the normal surface stresses shown in equation 2.16, on the specimen surface, root defects can be easily detected.

\[
\sigma_{11} = \frac{M_f z}{I}
\]

(2.16)

The constant bending moment, and therefore constant normal surface stress (Equation 2.16), was used for calibration of FBG sensors. The space available between the inner rollers may be used for calibration of sensors with different gauge lengths.
2.6.1.2 Tensile test

This kind of experiment is performed in a high number of contexts. Tensile tests define some of the most important and well known material parameters, such as its stiffness, yield strength, rupture strength and elongation. Before fatigue characterisation these tests are also necessary for definition of stress levels for example, since these levels are mostly defined in terms of percentage of the material yield strength. Furthermore, the relatively low cost and short execution time make these kind of experiments interesting as the first tests to be performed before further experiments are planned.

Among others depending on the parameters that need to be determined, different specimen designs may be chosen. For this thesis, the ASTM E8M [170] standard and the Deutsches Institut für Normung (DIN) standard 50125 [171] were used. If only the rupture strength is required, for example for lap joints, even a straight material stripe may be used.

2.6.1.3 Crack initiation test

The $S$-$N$ curve, or Wöhler curve, is probably one of the best known ways to characterise the fatigue life of specimens. ASTM standard E466, E467 and E468 [172–174] contain possible testing procedures. In this type of tests, a specimen is loaded by a harmonic function at constant frequency and amplitude until it breaks. The final number of cycles for a given stress level defines one point on this curve. At least three specimens at each level should be tested, preferably more, since fatigue life can have great variations.

In the $S$-$N$ curve approach, structural details, like butt-welding, are grouped into categories sharing a common resistance to fatigue and associated $S$-$N$ curve. These curves
give the allowable stress range (S) for a particular detail and material in terms of obtainable fatigue life (N), where the stress range is the nominal stress perpendicular to the crack surface, not considering local stress concentration factors [175].

In current codes and standards on fatigue strength assessment of conventional fusion welded joints and parent material, fatigue design curves are obtained by taking into account the scatter of fatigue behaviour and providing a reference curve with a probability of survival greater than or equal to 97.7% [176].

Data characterising the scatter is therefore required, and the present work includes experimental results for different laser beam welded aluminium alloys.

2.6.1.4 Crack propagation test

The C(T) specimen crack growth test aims to measure the crack growth rate. Therefore a specimen’s crack is measured at predefined intervals during crack growth. The ASTM E647 standard [177] should be followed thoroughly in order to provide confidence in the results. The number of tests necessary to define material properties depends on the material and on the accuracy of the measurements made.

C(T) specimens may present economy of material when compared with middle tension specimens and still provide a relative wide range of measurable crack lengths and stress intensity factor values. There is however no guarantee that the stresses determined in these C(T) specimens and the structure from where they were extracted are the same [178]. C(T) specimens provide a relatively inexpensive way to assess fracture mechanics parameters necessary for fatigue life estimations due to their smaller overall dimensions when compared with, for example, centre cracked plates. In order to augment the accuracy of these estimations, residual stresses near the crack should be known in the specimen used for material behaviour characterisation and in the complete structure. This is especially true for larger size specimens, since by cutting smaller specimens, more residual stresses are relaxed.

2.6.1.5 Crack propagation test under biaxial loading

Biaxial fatigue testing is important, since real-life service loading conditions can be better simulated. The aircraft fuselage shell should be considered as a pressure vessel subjected to additional external loads. Therefore significant in-plane loads are present in the longitudinal and circumferential directions. Biaxial fatigue testing is however a complex effort, since special equipment is needed, and the experimental setup is complex and expensive.
Furthermore no standard procedure for fatigue crack characterisation under biaxial loading conditions exists so far, therefore different investigations in this area are presented below in order to allow the definition of a reasonable testing procedure.

Perhaps the simplest way to perform mechanical tests in biaxial stress states is to use uniaxial specimens which introduce bi-axial stress states, for example through an inclined initial notch. This approach does however not allow to perform extended tests, since the biaxiality ratio rapidly changes during the experiments. Hannon and Tiernan presented several approaches for biaxial testing systems and specimen designs, mostly for static load experiments [179]. Most of this information may be adapted for fatigue tests as well. Machine designs of different complexity have been developed, from adaptation of uniaxial testing machines to tailor made multi-axial machines, and used for testing a variety of materials. Atkins et al. presented a special fixture which creates stress biaxiality in cruciform polymeric specimens by using a standard uniaxial testing machine with different biaxiality ratios [180]. A similar approach was also used by Abu-Farha et al. [181] who studied various geometries for static biaxial tests at elevated temperature. It should be remembered that with this kind of setup the ratio between both axes is necessarily constant during the whole test, which may not be intended. Biaxial fatigue resistance of polymeric plates was studied using a special fixture, which transforms the uniaxial compressive loads from a standard testing machine into biaxial tensile loads [182]. A similar system which adapts uniaxial testing machines for biaxial experiments was developed by Bhatnagar et al. [183]. This system is capable of applying any load relation between the two axes. Some test machines are available on the market and have been used successfully, for example testing the thermoelastic approach for stress intensity factor determination in a biaxial stress field [184], whereas some researchers used tailor made equipment [185]. Armentani et al. used a custom made loading frame for complex shape biaxial fatigue specimens. A full-scale stiffened glass fibre reinforced aluminium panel was tested [186]. For the works presented hereafter the equipment was made by Instron [187], and controlled by several interconnected servo-hydraulic controllers. Comparable systems with four independent actuators were already used successfully before [188, 189].

The specimen design is also challenging, and since no standard geometry exists yet, comparison of results among different laboratories is difficult [179]. Dalle-Donne et al. showed different biaxial specimen design used at the Deutsches Zentrum für Luft- und Raumfahrt (DLR) [190]. Smits et al. showed the advantages of using four actuators for cruciform specimens [191] and compared different specimen geometries of small composite specimens. He concluded that by reducing the thickness in the central gauge region and using a fillet between two adjacent arms, failure would more likely occur in the centre of the specimen. While most of the cruciform biaxial experiments are made with
thin sheets with differences in geometry and size, Bass et al. shows a biaxial specimen with a thickness of over 100mm for studies related to the application in a nuclear reactor pressure vessel [192]. For biaxial fatigue, good results were obtained with a specimen with around 400mm and a corner radius of 25mm [193]. In this way no secondary cracks were detected. Simulations concerning the detailed stress distribution in this kind of specimens were performed by Lamkanfi et al. [194]. The continuous interest in this kind of specimen is proven by the fact that initial FEM analyses were already performed in 1971 [195]. Recently Makris et al. presented a numerical specimen geometry optimisation methodology which should lead to easier biaxial test setups [196]. This shows that this kind of experiments is still in a relatively early development stage.

The ideal boundary conditions for biaxial tensile specimen are such that a tensile load can be applied in one direction without generating any resistance in the perpendicular direction. Obviously such an ideal boundary condition does not exist in the laboratory. Hence, various flat cruciform specimens are explored with the same objective to transfer the load into the central gauge section as close to the ideal condition as possible. The more commonly used specimens do have a square central gauge zone plus four long loading arms. To help the uniform load transfer (load diffusion), two types of specimens have been used. One type has reduced thickness in the central gauge section, and the other introduces a number of slots in the loading arms of the specimen [179, 197, 198]. A detailed study based on a photo elastic analysis was performed by Pisarenko et al. concerning the number of slots to be used in this kind of specimens. This author found that the higher number of slots lead to the best stress distribution in the specimen centre [199]. Some researchers combine both types in their test specimens [200, 201], as shown in Figure 2.7.

Concerning the design of the load-diffusion slots in the loading arms of cruciform specimens differs, an odd number of slots was found to work better in reducing the local stresses [202]. In another study [203], detailed Finite Element Analysis (FEA) demonstrated that these slots can improve the stress uniformity and increase the stress level (closer to the intended loading level) in the centre test zone. It was found that the slot width and number have significant influence, whereas the slot length and location have little effect on the stress distribution in the central test zone.

2.6.2 Metalurgical characterisation

A metalurgical characterisation is interesting for joining technologies by welding processes in order to detect microstructural differences introduced by these joining technologies. In another context, it may also be of interest to characterise the grain orientation.
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and size in rolled sheet metal.

2.6.2.1 Microhardness

Microhardness measurements allow defining the extent of the Heat Affected Zone (HAZ) in fusion welding and additionally the TMAZ in FSW. Hardness is expected to have variations in these zones.

2.6.2.2 Metallography

Macrographic images provide an overview over the extent of deposited material in welds. Since in most cases different materials are used for deposition in fusion welding, this images provide a good way of comparing the extend of the influence of different welding techniques. Aluminium alloys, essentially when welded without filler material, are more
difficult to analyse using this technique due to low contrast between the welded zone and base material.

Micrographic images show the microstructure of metals and other materials. This makes it possible to compare grain constitution and size of the base material and the weld bead. The uniformness of material after welding can be seen by this analysis.

Fractography is the study of the features of fracture surfaces. It is useful for determination of the extend of the fatigue cracking zone and final ductile rupture zones. Material defects which lead to crack initiation may also be found. This analysis may be performed both by Optical Microscopy (OM) and Scanning Electron Microscopy (SEM). While OM allows to get an overview over the fracture surface and for example the extension of the fatigue zone, the SEM analysis allows to go further into fracture details.

### 2.6.2.3 Scanning Electron Microscopy

Scanning Electron Microscopy equipment is a type of electron microscope that images samples by scanning it with a high-energy beam of electrons in a raster scan pattern. The electrons interact with the atoms of the sample producing signals that contain information about the samples surface topography, composition, and other properties such as its electrical conductivity [204].

When used for fractography analyses, the SEM allows to see details such as ductile fracture dimples or fatigue striations. Up to a certain extent these striations may be used for determination of the fatigue crack growth rate [205, 206]. While in some situations this seems to be possible [207], in others, especially for very low crack growth rates, authors have found that there is no direct relation between crack growth rate and fatigue striation spacing [208, 209]. It is therefore a complex task to perform post-mortem fracture analyses, since the striation spacing may not represent the macroscopic fatigue crack growth rate.

### 2.6.2.4 Chemical composition

The chemical composition of some material may for example be determined using an SEM equipment with X-ray tube. The analysis of the chemical constituents of a material allows for example the comparison of differently named aluminium alloys. Different manufacturers may choose to use different naming schemes for their alloys, but the chemical composition should stay comparable and within specified limits among different manufacturers.
2.7 Damage tolerance

Large parts of aeronautical structures, such as the fuselage panels, are designed taking into account damage tolerance concepts. This approach is based on the assumption that such structures may contain flaws up to a certain size and must withstand them at least until the next verification. This design principle allows the construction of lighter components without sacrificing the occupants safety, but at the cost of a more advanced maintenance system. A careful balance between safety and economy has to be found [210].

The SIF concept is a way of determining the danger of an existing crack. Both the propagation direction and speed may be calculated based on the knowledge of the SIF. Normally this parameter is analytically or numerically calculated.

A multipoint overdeterministic method may for example be used for SIF calculation, where experimental data collected from optical images is fitted to Muskhelishvili’s equations describing the stress field around the crack tip [211]. The procedure is based on the overdeterministic approach, used previously in fracture mechanics for processing photoelastic data in experimental determination of SIFs [212]. The values of the stresses in an unlimited number of points around the crack tip can be used in order to fit a multi-term series expansion of the stress field. A system of equations is obtained in which the coefficients of each term are the unknowns. The number of equations is equal to the number of the considered points while the number of unknowns is equal to the number of terms chosen in the series expansion, which is much lower. This overdeterministic method has the advantage of being able to use an unlimited number of data points, thus minimising the error. Pastrama et al. [213] used this approach to fit stress values obtained from finite element analyses in order to obtain SIF at the tip of a radial crack in a cracked plate and in a thick walled tube subjected to internal pressure.

A detailed description of the theoretical framework was written by [214] which used a similar approach, although applied to the measured displacement field around the crack tip and not to the strain field as in the present case. The differences in the determined SIF found in relation to the used reference were comparable to the present work. The same technique was also used by [215] to determine the SIF in a high speed imaging application. Mc Neill et al. [216] used DIC to solve the Westergaard solution for the SIF using the displacement field around the crack tip. One approach to eliminate the rigid body motion based on the displacement field is presented by [217] where the rigid body motions are treated as further unknowns in the over determined system of equations to be solved.
Other approaches include energetic methods to determine the SIF based on the displacement field, as demonstrated by Roux or Hamam for example [218, 219]. These methods mostly have numerical origins.

2.8 Design of experiments

Design of Experiment (DoE) techniques have been widely used for the last two decades in order to gather information regarding the correct input parameters for obtaining the desired output. Well designed experiments reduce the effort necessary to invest in order to obtain the required data. Experimental optimisation techniques compared by Benyounis [220] are shown in Table 2.5. The experimental optimisation techniques mostly aim at obtaining the parameters necessary, for example for models, with the lowest possible effort.

<table>
<thead>
<tr>
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<th>Tagushi method</th>
<th>Full factorial design</th>
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<tr>
<td>Computational effort</td>
<td>medium</td>
<td>low</td>
</tr>
<tr>
<td>Accuracy</td>
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<td>very high</td>
</tr>
<tr>
<td>Experimental domain</td>
<td>any</td>
<td>regular</td>
</tr>
<tr>
<td>Understanding</td>
<td>moderate</td>
<td>easy</td>
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</tbody>
</table>

The fractional factorial technique was successfully used for predicting weld bead geometry while using the welding parameters as inputs [220]. The contribution of each factor to the result may be seen in this kind of models.

The Taguchi method is known to be a powerful optimisation technique for improving quality at low cost. Its approach of comparing signal-to-noise ratios for each combination of input and output parameters may lead to non-optimal solutions. It is also a less flexible technique which can lead to needless experiments [220]. For a fusion welding technique, Figueiredo et al. used the Taguchi technique for planing the experiments [221], and has thereby successfully reduced the number of required experiments. Louro et al. applied the Taguchi technique to the definition of FSW parameters [222]. He noted that while this technique provides a good way for defining parameters even in complex situations, no extrapolation outside of the tested parameters should be performed.
2.9 Modelling

It is the author’s opinion that modelling should be used as a tool for helping to understand real phenomena in engineering applications, and never for the sake of modelling.

While modelling is absolutely necessary in the high technology industry nowadays, and definitely leads to savings in cost and time, it should not be forgotten that all models depend on the introduced parameters and on the used assumptions to simplify a real world problem. Models have therefore always to be verified by experimental results in order to be valid. Modelling may be used as a tool to understand complex relations, which may not be easily inferred by simple experiments. Complicated mechanisms may be better understood using models.

To the author’s view, calibrating or fitting numerical models with meaningless parameters until some specific result is obtained, should not be done due to the lack of capability to represent the real problem accurately. Instead, only one specific experiment - the one used for calibration - is represented with high accuracy, while in most cases, no guarantee may be given for extrapolated results or results for other experiments. Most of the time, when very complex models are built for multi-physics processes such as FSW, the main problem is to obtain the necessary material parameters in a broad range of temperatures. These parameters have to be determined experimentally with a very high effort. Often it is not even possible to obtain all necessary parameters experimentally, and as soon as more than one parameter is missing, the fitting of a model to experimental results may introduce errors in the model which are not immediately noticed due to the high degree of accuracy for a specific experiment.

Only a validated model has the potential to produce reliable information about thermal history, residual stress and mixing pattern in FSW. It is however not easy to build such a model due to the complex interaction between the FSW tool and workpiece surfaces, including thermal and material flow aspects for example. The difficult part of creating a model for such a complex process is not the ability to simulate one specific experiment, where parameters are often chosen almost arbitrarily in order to achieve good results, but to obtain a model capable of reliably simulating unknown conditions. This ability would improve the FSW process, since experiments could be cheaper and faster made numerically and later validated experimentally.

Keeping in mind these general thoughts about the application of modelling techniques to complex situations such as FSW, some interesting approaches exist which are presented hereafter. It is to be expected, that in the near future models evolve to a point where they may be used to increase productivity and quality in the industry for example. Dong reviewed the state of the art of welding residual stress and distortion modelling
He very well notes that numerical models should be used to help engineers to better understand complex problems in order to be able to find effective engineering solutions for real world problems. Experimental approaches are still fundamental for most engineering applications.

Modelling of welding distortions and residual stresses in fusion welds has been thoroughly studied and partly good results have been obtained [78, 79, 139], but for friction stir welds the situation is more complicated, since additionally to the heat introduced by the welding process, the complex material flow has to be considered for precise results. For complex situations, such as the simulation of friction stir welding, investigations reported in the last 20 years have taken two main directions - the development of complex models including most of the phenomena occurring during welding, and simplified models, directed to industrial applications [142]. A common problem of manufacturers is to obtain a joint with good mechanical properties and minimal detrimental residual stress and distortion by controlling the welding input parameters only [220]. Several different approaches may be taken to help in this aspect, among them are carefully designed experiments and numerical models which may be used by manufacturers for welding parameters definition. Normally the combination of various of the techniques presented in this section is used to obtain good results [220]. Table 2.6 shows a comparison between different modelling techniques performed by Benyounis [220].

Table 2.6: Comparison of different modelling techniques [220].

<table>
<thead>
<tr>
<th></th>
<th>ANN</th>
<th>GA</th>
<th>RSM</th>
<th>FEM</th>
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<tr>
<td>Computational effort</td>
<td>low</td>
<td>very high</td>
<td>low</td>
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<td>medium</td>
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<td>Experimental domain</td>
<td>any</td>
<td>any</td>
<td>regular</td>
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<tr>
<td>Understanding</td>
<td>moderate</td>
<td>difficult</td>
<td>easy</td>
<td>moderate</td>
</tr>
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</table>

2.9.1 Artificial Neural Networks

Artificial Neural Networks are numerical models which try to replicate the biological neural network. They are used in order to solve complex problems in various applications where no linear relationship between input and output can easily be found. Artificial Neural Networks (ANNs) are very capable of recognising patterns in input data which can afterwards be used to predict outputs. Such a system is comprised of three types of layers. The input layer, where all the input factors are inserted, the hidden layers, where the data is processed and the output layer, where the final output vector is processed. Artificial neural networks are built by basic units called neurones, which are
interconnected by synapses. To each synapse a weight factor is associated. During the training period with known input and output data, the network learns the patterns to be recognised later on, being able to predict results based on new input data.

One property, whether it is positive or negative remains to be seen, is the fact that this modelling technique does not need any principles of physics directly. It needs however the input of good data for learning purposes, which may come from numerical models or experimental analysis. Here a good knowledge of the process to be studied is required in order to correctly choose the learning data.

The ANN modelling process is composed by the five steps. Initially a database collection has to be created. This data has to be analysed and pre-processed. Afterwards the neural network may be trained based on the available data. Testing of the trained network and possibly further training is required before being able to use use the trained ANN for simulation and prediction.

ANNs have been shown to perform better than Response Surface Methodology (RSM) and other techniques, especially when highly non-linear behaviour is the case [75, 220]. It was also shown that ANNs provide little to no information about the design factors and their contribution to the result if no further analysis is performed [220]. Due to their light mathematics, ANNs can lead to real-time result with equal or even better accuracy and reliability than traditional data analysis algorithms [220]. This leads to the possibility of using this kind of models in automated processes for process control for example.

Okuyucu developed an ANN capable of correlating FSW parameters with the corresponding mechanical properties [66]. For training purposes, 20 specimens were welded and characterised mechanically. The back-propagation learning algorithm was used with feed-forward in the ANN with one hidden layer with seven neurones and numerical optimisation techniques. Relatively good predictions were obtained using this technique, and it was possible to show the capability of learning from examples and the simplicity and speed of calculation of this kind of model. Using as inputs the tool rotation speed and weld speed, the tensile strength, yield strength, elongation, hardness of weld metal and hardness of the HAZ could be predicted with a $R^2$ value of over 98.5%.

Yousif used two hidden layers with three neurones in the first and 6 in the second trained by Levenberg Marquardt algorithm [223]. Good performance was found for the prediction of tensile stress, being the error below 2%. Bending stress and elongation showed errors of over 7 and almost 12%. The higher error may be explained by the fact that in this case only 10 data sets where used for training instead of 20 used by Okuyucu [66].
It was found that internal stresses may influence hardness measurement results; hardness decreases with internal tensile stresses, but does not increase the same amount with internal compressive stresses, since the effect is non-linear. This effect was analysed using neural networks in [224], and one conclusion drawn was that it is useful to estimate the error in hardness measurement induced by the residual stress state of the analysed specimen. It was shown that indentation tests may partially be used for tensile residual stress estimation in some cases.

2.9.2 Genetic Algorithms

As pointed out by Benyounis [220], genetic algorithms operate simultaneously with a huge set of search space points for finding the optimal solution, while other methods are bound to only one point. A negative point for this method is shown to be the high computational weight and the requirement for a good setting of its parameters.

In the area of aircraft design, Antoine et al. used, among others, the Genetic Algorithm (GA) to explore the inclusion of environmental performance in aircraft design [225]. Liao used the GA to optimise the weld sequence for sheet metal assembly with the goal of reducing the final part distortion resulting from random manufacturing tolerance variations [226]. Mollah and Pratihar applied the GA to the optimisation of a model concerning the Tungsten Inert Gas (TIG) welding process. Park and Rhee used a GA to optimise various consequences of the laser welding parameters for aluminium alloys in the automotive industry simultaneously. Multi objective optimisation applied to aircraft maintenance was explored by Wang et al. using a modified GA. Tansel et al. applied GAs to the optimisation of ANNs for FSW parameter estimation [77].

2.9.3 Response Surface Methodology

The Response Surface Methodology methodology can help to develop an approximation of the true functional relationship between independent input variables and the output variable. This method is good for optimising a response variable created by several independent input variables. According to [75], it is possible to identify which input variables are important and which do not influence the result variable significantly.

The RSM was compared to an ANN by Lakshminarayanan [75] for the prediction of the tensile strength of a FSW joint of AA7039. It was found that small variations in rotational speed, cause large changes in tensile strength, while changes in welding speed and axial force influence the tensile strength to a lesser extent.
Chapter 2. State of the art

The RSM was found to be superior to ANNs and GAs in cases where a large number of experiments is not affordable. Furthermore, it has the ability to show the factor contribution of the inputs [220] and is therefore of higher interest for result interpretation.

2.9.4 Finite Element Method

As suggested by Aarbogh [227], precisely modelling stresses and deformations induced during welding of phase transformation steels requires constitutive equations quantifying the flow stress during the visco-plastic deformation of the material. Mostly, complicated experiments are needed for their determination. More simplistic models may be obtained, based on the ideal plasticity approach and on fitting the flow stress to Satoh test results obtained for a representative temperature cycle for the HAZ. Acceptable results were found with such simplified models [227].

Rajamanickam made a non-linear three dimensional thermal model using Ansys, which is capable of estimating the thermal history of a butt FSW joint [43]. Therefore temperature dependent thermal properties of the AA2014 alloy were used. The temperature distribution was calculated by applying the differential equation governing heat conduction in a solid body. This simulation involves a number of arbitrary constants to be specified by initial and boundary conditions. For the specific experimental case which was simulated, good agreement was found. The temperature cycle during FSW was also studied by Chao et al. [228] successfully.

Kamp et al. predicted the precipitate evolution and strength in friction stir welded and post-weld heat treated aluminium alloy AA7050 as a function of rotational and advancing speed. Therefore coupling between a thermal, microstructural and yield strength model were required. Each of these models has an associated error, but nevertheless the authors refer an average error below 20% for the strength prediction [229].

Liu and Zahng simulated welding angular distortion including external restraints for the asymmetrical double sided arc welding method. They found that welding residual stress with a restrained joint is larger than with an unrestrained joint [47]. Distortion was successfully simulated using a multi-body coupling finite element model, and the conclusion was drawn that the transient angular distortion (with applied restraints) decreases quickly with an increasing distance of the restraining force to the welding line. The residual distortion however, can not be eliminated and lowest values are obtained for a combination of high clamping force and medium distance to the welding line. As will be later seen, the residual stress distribution in FSW with heavily and loosely restrained workpieces is significantly different, as well as the residual distortion. This seems to be due to the equilibrium between distortion and residual stress for a certain energy
input into the joint. Keeping all parameters besides clamping constant, when higher
distortions are measured, lower residual stresses may be expected as a consequence.
Chapter 3

Material Characterisation

The present chapter concerns the material characterisation of all materials used throughout the experimental work. Information regarding chemical composition, static tensile properties as well as cyclic loading results are given.

AA7075-T73 used in the context of studying the influence of residual stresses on distortion when machining complex structures was analysed. Tensile properties in 3 directions are given for different stretching grades. Through the thickness microhardness measurements provide an insight into the materials homogeneity. A microstructural analysis was performed in order to determine grain properties in rolling and transversal directions.

AA6082-T6 was analysed in the context of Fibre Bragg Grating (FBG) sensor instrumentation. This alloy was chosen due to its wide availability. Tensile test results are shown for base material and Friction Stir Welding (FSW) joints.

In the aeronautical industry, aluminium-lithium alloys are currently of high interest due to their lower density and good mechanical properties. The third generation of these alloys also has a high toughness, but problems with the weldability have to be studied. FSW was found to be one possible solution. The aluminium-lithium alloy AA2198-T851 was used in the context of the influence of clamping force on distortion and residual stress during FSW and for biaxial fatigue tests. Tensile, as well as metallurgical, properties were determined.
3.1 AA7075-T73

For the aluminium alloy AA7075 in the T7 condition, the base material properties shown in Table 3.1 may be found in the literature. While the proportional yield stress for 0.2% is not exactly the same as the real yield stress, for the purpose of the current work, these parameters were considered identical.

<table>
<thead>
<tr>
<th>Source</th>
<th>(\sigma_{YS}) [MPa]</th>
<th>(\sigma_r) [MPa]</th>
<th>(E) [GPa]</th>
<th>(\varepsilon) [%]</th>
<th>(K_{app}(T-L)) [MPa√m]</th>
<th>(K_{app}(L-T))</th>
</tr>
</thead>
<tbody>
<tr>
<td>Lanema [9]</td>
<td>435</td>
<td>505</td>
<td>72</td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>MatWeb [6]</td>
<td>435</td>
<td>505</td>
<td>72</td>
<td>13</td>
<td>20</td>
<td>32</td>
</tr>
</tbody>
</table>

The alloy AA7075-T73 was used in four different stretching conditions. The difference in mechanical and metallurgical properties is small, mostly within the expectable scatter for these kind of measurements. The difference between the different grades is mainly expected in the residual stress distribution through the plate thickness.

3.1.1 Mechanical characterisation

Tensile tests were performed according to DIN50125 [171] on a Zwick&Roell mechanical tensile testing rig. As can be seen in Tables 3.2 to 3.4, no differences in the mechanical properties in different directions to the rolling direction could be detected. The material is therefore to be considered homogeneous. Table 3.2 and Figures 3.1 show the tensile properties for a stretching grade of 1.5%.

<table>
<thead>
<tr>
<th>(E) [GPa]</th>
<th>(\sigma_{YS}) [MPa]</th>
<th>(\sigma_r) [MPa]</th>
<th>(\varepsilon) [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>(\bar{x})</td>
<td>(\bar{s})</td>
<td>(\bar{x})</td>
<td>(\bar{s})</td>
</tr>
<tr>
<td>0°</td>
<td>71</td>
<td>0.98</td>
<td>475</td>
</tr>
<tr>
<td>45°</td>
<td>70</td>
<td>0.54</td>
<td>462</td>
</tr>
<tr>
<td>90°</td>
<td>71</td>
<td>0.34</td>
<td>474</td>
</tr>
</tbody>
</table>
Figure 3.1: Tensile test on AA7075-T73, 1.5%.

Table 3.3 and Figures 3.2 show the tensile properties for a stretching grade of 2.0%.

Table 3.3: Mechanical properties for a stretching grade of 2.0%.

<table>
<thead>
<tr>
<th></th>
<th>E  [GPa]</th>
<th>$\sigma_Y$ [MPa]</th>
<th>$\sigma_T$ [MPa]</th>
<th>$\varepsilon$ [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>$\bar{x}$</td>
<td>$s_x$</td>
<td>$\bar{x}$</td>
<td>$s_x$</td>
</tr>
<tr>
<td>$0^\circ$</td>
<td>70</td>
<td>0.54</td>
<td>468</td>
<td>1.43</td>
</tr>
<tr>
<td>$45^\circ$</td>
<td>70</td>
<td>0.30</td>
<td>455</td>
<td>0.55</td>
</tr>
<tr>
<td>$90^\circ$</td>
<td>72</td>
<td>0.99</td>
<td>476</td>
<td>1.42</td>
</tr>
</tbody>
</table>
Figure 3.2: Tensile test on AA7075-T73, 2.0%.

Table 3.4 and Figures 3.3 show the tensile properties for a stretching grade of 2.4%.

Table 3.4: Mechanical properties for a stretching grade of 2.4%.

<table>
<thead>
<tr>
<th></th>
<th>E [GPa]</th>
<th>σYS [MPa]</th>
<th>σT [MPa]</th>
<th>ε [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>0°</td>
<td>70</td>
<td>68</td>
<td>532</td>
<td>0.29</td>
</tr>
<tr>
<td>45°</td>
<td>70</td>
<td>40</td>
<td>527</td>
<td>0.55</td>
</tr>
<tr>
<td>90°</td>
<td>71</td>
<td>93</td>
<td>540</td>
<td>1.20</td>
</tr>
</tbody>
</table>
3.1.2 Metallographic characterisation

Microhardness measurements were performed in the specimens in order to identify possible influences of the rolling process on the through-the-thickness microhardness distribution of the plates. Therefore 5 points were measured in the centre of the plate for reference and their average value was calculated. Afterwards, 12 points were measured through-the-thickness.

The microhardness variations are represented in Figure 3.4(a) for the rolling direction, and Figure 3.4(b) orthogonally to the rolling direction for a stretching grade of 1.5%.

The measured average hardness was 163HV in rolling direction and 162HV orthogonally to the rolling direction, and there was no noticeable variation across the thickness of the plate.

The microhardness variations are represented in Figure 3.5(a) for the rolling direction, and Figure 3.5(b) orthogonally to the rolling direction for a stretching grade of 2.0%.
The measured average hardness was 161HV in rolling direction and 158HV orthogonally to the rolling direction, and there was no noticeable variation across the thickness of the plate.

The microhardness variations are represented in Figure 3.6(a) for the rolling direction, and Figure 3.6(b) orthogonally to the rolling direction for a stretching grade of 2.4%.

The measured average hardness was 164HV in rolling direction and 163HV orthogonally to the rolling direction, and there was no noticeable variation across the thickness of the plate.
Figure 3.6: Microhardness of AA7075-T73, 2.4%.

The different microstructures through the thickness of the sheet are represented in Figures 3.7 to 3.9 for the stretching grades of 1.5%, 2.0% and 2.4% respectively.

As can be verified, no clear difference in the microstructure between the different stretching grades can be recognised.
Chapter 3. Material Characterisation

Figure 3.7: Micrography of AA7075-T73, 1.5%.
(a) A - Top; Orthogonally to the rolling direction; 200x magnification
(b) D - Top; Rolling direction; 200x magnification
(c) B - Middle; Orthogonally to the rolling direction; 200x magnification
(d) E - Middle; Rolling direction; 200x magnification
(e) C - Bottom; Orthogonally to the rolling direction; 200x magnification
(f) F - Bottom; Rolling direction; 200x magnification

Figure 3.8: Micrography of AA7075-T73, 2.0%.
Chapter 3. Material Characterisation

Figure 3.9: Micrography of AA7075-T73, 2.4%.
3.2 AA6082-T6

For the aluminium alloy AA6082 in the T6 condition, the base material properties shown in Table 3.5 are found in the literature and compared to experimental data. Tensile tests of the specimen base material in rolling direction were carried out to obtain the stress/strain curve and the major mechanical properties, using a 250kN servo-hydraulic MTS Systems Corporation (MTS) testing machine and a gauge length of 25mm. Isotropic behaviour is expected in this alloy. The American Society for Testing and Materials (ASTM) E8M standard was followed for the measured values shown in Tables 3.5 [230] and 3.6 (Chapter 4). The measured results are shown graphically in Figure 3.10.

Table 3.5: Mechanical properties of AA6082-T6.

<table>
<thead>
<tr>
<th>Source</th>
<th>σ_Ys</th>
<th>σ_R</th>
<th>E</th>
<th>ε</th>
</tr>
</thead>
<tbody>
<tr>
<td>Peel et al. [13]</td>
<td>301</td>
<td>0.96</td>
<td>337</td>
<td>1</td>
</tr>
<tr>
<td>Alcoa [231]</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Lanema [9]</td>
<td>310</td>
<td>340</td>
<td>70</td>
<td></td>
</tr>
<tr>
<td>MatWeb [6]</td>
<td>250</td>
<td>290</td>
<td>10</td>
<td></td>
</tr>
<tr>
<td>Measured [230]</td>
<td>277</td>
<td>1</td>
<td>323</td>
<td>67</td>
</tr>
</tbody>
</table>

Table 3.6: Mechanical properties of FSW AA6082-T6.

<table>
<thead>
<tr>
<th>Source</th>
<th>σ_Ys</th>
<th>σ_R</th>
<th>E</th>
<th>ε</th>
</tr>
</thead>
<tbody>
<tr>
<td>Measured</td>
<td>155</td>
<td>10</td>
<td>237</td>
<td>65</td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
</tbody>
</table>
3.3 AA2198-T851

For the aluminium alloy AA2198 in the T8 condition, the measured base material properties are compared to values found in literature in Table 3.7. As can be seen, in rolling direction higher performance is obtained in this alloy.

<table>
<thead>
<tr>
<th>Source</th>
<th>$\sigma_Y$ (MPa)</th>
<th>$\sigma_r$ (MPa)</th>
<th>E (GPa)</th>
<th>$\varepsilon$ (%)</th>
<th>$K_{app}$(T-L) (MPa$\sqrt{m}$)</th>
</tr>
</thead>
<tbody>
<tr>
<td>0$^\circ$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alcan [26]</td>
<td>470</td>
<td>510</td>
<td></td>
<td>12</td>
<td></td>
</tr>
<tr>
<td>Cavaliere et al. [10]</td>
<td>436</td>
<td>491</td>
<td>77</td>
<td>16</td>
<td></td>
</tr>
<tr>
<td>Measured</td>
<td>475</td>
<td>516</td>
<td>1</td>
<td>13</td>
<td>3</td>
</tr>
<tr>
<td>90$^\circ$</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Alcan [26]</td>
<td></td>
<td></td>
<td></td>
<td></td>
<td>140</td>
</tr>
<tr>
<td>Cavaliere et al. [10]</td>
<td>347</td>
<td>431</td>
<td>74</td>
<td>20</td>
<td></td>
</tr>
<tr>
<td>Measured</td>
<td>450</td>
<td>499</td>
<td>1</td>
<td>71</td>
<td>7</td>
</tr>
</tbody>
</table>

The base material characterisation shown in Figure 3.11 of AA2198-T851 was performed using a gauge length of approximately 30mm, measured by a laser probe. Three specimens were tested for each configuration.
Figure 3.11: Tensile tests performed on AA2198-T8 base material in two orthogonal directions.

Tensile properties of the welded alloy depending on the applied clamping force will be shown in Chapter 5.
Chapter 4

Fibre Bragg Grating sensors for welding monitoring

In this chapter the application of FBG sensors in welding monitoring is explored, optimising the sensor application techniques and calibration procedures, and systematically comparing FBG measurements with data obtained using alternative techniques. The work was developed in the context of the project entitled “WELDING - Fiber Bragg grating sensors for modelling of Friction Stir Welding and Laser Beam Welding” which was concerned with the usage of fibre Bragg grating sensors for measurement of temperature and strain fields in advanced welding processes such as FSW and Laser Beam Welding (LBW).

Various experiments were performed in order to determine the best possible way for installation of the sensors, including adhesive selection, adhesive application, curing and post-curing procedures for the bonded sensors and also accommodation of the sensors on the specimen using mechanical or thermal loads.

Calibration constants exist for the sensors under certain conditions. Calibration procedures were proposed which lead to a better knowledge of the real coefficients necessary for the translation of the Bragg wavelength shift into strain and temperature based on the application techniques proposed for welding monitoring.

Finally, instrumented welding experiments were performed with both the well known but aggressive Metal Inert Gas (MIG) and the more recent FSW processes. Good measurement results were obtained for both processes, which lead to the conclusion that FBG sensors using the proposed instrumentation techniques are suitable for the task of welding monitoring.
4.1 Selected equipment and installation procedures

4.1.1 FBG sensor

FBG sensors may be produced with several grating lengths, typically between 5 and 200mm [151], the longer gratings being particularly interesting for distributed sensing applications. In the present case the grating length was chosen to be short, since measurements were taken at points and not along lines. Furthermore, the available laser equipment and phase mask limit the possible grating lengths to approximately 5 to 30mm. When the strain gradient in a grating is too high, the wavelength peak will not only shift, but it will be divided, thus reinforcing the need for shorter grating lengths. Therefore 10mm long gratings, which are known to work, were compared to 5 mm long gratings for better results in the present case.

4.1.1.1 Comparison of different grating lengths

One of the possible problems that may arise due to the wrong grating length choice, is that the FBG signals get corrupted. When a strong gradient is to be measured, the grating may be divided inside of its gauge length. To the interrogation system this will seem like there are suddenly two sensors instead of one. The used acquisition system is therefore not capable of correctly analysing the received reflections. Figure 4.1 illustrates what may happen if the grating length was chosen too long for a welding experiment with high thermal and strain gradients.

![Figure 4.1: Strain measured during a MIG welding experiment using grating lengths of 5 and 10mm.](image)
As can be verified, the sensors with the shorter grating length are able to withstand the strong gradient and actually measure the average strain in the gauge length area. The gratings with the 10mm gauge length are not capable of accompanying the surface strain, and eventually stop measuring correctly after relatively low average strains, due to the separation of the original grating in various gratings.

Due to the high thermal and strain gradients found in welding applications, and knowing that a shorter sensor will lead to better results, only the shorter gratings should be used for welding monitoring.

When the strain gradient is too high in a grating, the wavelength peak will not only shift, but it will be divided, making an automated interpretation of the results very difficult, as is shown in Figure 4.2.

![Figure 4.2: Effect of a strong gradient inside the measurement area of the FBG sensor when subjected to an external load.]

Since the present work is not intended to study distributed sensors, only the 5mm long gratings were chosen for the welding experiments. The capability of distributed sensing using longer gratings should however be kept in mind for future applications with high strain and temperature gradients.

4.1.1.2 Comparison of different application techniques

For surface application, it is necessary to guarantee a good transmission of the physical quantities to be measured to the sensor itself. This is an important aspect which was modelled by Duck et al. [232], who also mentioned that less steep gradients lead to better strain transfer which reinforces the idea to use shorter grating lengths for a lower influence of the steep gradient on the measurements. For strain sensors this means that the sensor has to be adequately bonded along the complete grating length using a sufficiently flexible adhesive which is still stiff enough for transmitting all surface strains.
to the fibre. While one approach would be the use of a ceramic adhesive as used by Suarez et al. [155], a better strain transfer from the plate surface to the sensing device can be obtained by the use of an epoxy based high temperature strain gauge adhesive. The inconvenience of this type of adhesive is however that temperatures should not be much higher than 200°C. A easier to apply cyanoacrylate based alternative, which can be used at room temperature and is not recommended for usage over 80°C, was also tested.

As was found in both MIG welding and FSW experiments, the temperature sensing device should not be completely bonded to the specimen in order to eliminate unwanted influences of the strain present on the specimen surface. Figure 4.3 shows the different signals obtained with both, a fully bonded temperature sensor, and a temperature sensor only bonded on its tips with a slight curvature in the measurement area for mechanical strain relaxation. This way only the thermal expansion of the fibre material is measured.

**Figure 4.3:** Effect of different bonding techniques on the temperature measurement results: sensor extension to be bonded to the specimen.

A four point bending experiment at constant room temperature was performed in order to be able to compare the performance of different bonding techniques for strain measurement in tension and compression at constant room temperature, see Figure 4.4.

Cyanoacrylate (CA) based adhesive was used in two variants. First only the extremities of the sensor where bonded to the surface to be measured as was done successfully for temperature measurements. Afterwards the same adhesive was used, but the whole measuring grid was bonded to the surface, in both cases keeping the sensor as straight as possible.
For comparison strain gauges were applied and a Finite Element Method (FEM) model was created.

![Graph showing the effect of different bonding techniques on strain measurement results.]

**Figure 4.4:** Effect of different bonding techniques on the strain measurement results: sensor extension to be bonded to the specimen surface.

Various conclusions may be drawn based on the obtained results. Firstly it should be mentioned that the zero for the different measuring instruments was set at the same load in the second increment, and therefore a direct comparison of the zero value of the FEM model should not be made. The evolution of the strain during the experiment should however be comparable.

The bonding technique to be used is definitely based on bonding the whole extension of the measuring grid. The CA bonded sensor which was only bonded on its tips was not able to measure compressive strains on the specimen surface. The goal of the project was to monitor surface strain and temperature during welding processes, therefore no attempt was made to embed the fibres inside the plates.

Also for surface application, it is necessary to guarantee a good transmission of the physical quantities to be measured to the sensor itself, as was mentioned before. In the present case the high temperature strain gauge adhesive M-Bond 600 from Vishay was selected for this task, since the cyanoacrylate based alternatives which can be used at room temperature are not recommended for usage over 80°C.

The temperature sensors on the other hand have to be insulated from strain effects, while retaining sufficient contact to the surface for a good heat transfer to the fibre. For this purpose, the fibre is only bonded to the surface with two dots guaranteeing, due to its small curvature, that no strain is transferred, and silver based thermal paste is applied around the grating for enhanced heat transfer.
The used bonding technique for strain and temperature measurements is shown schematically in Figure 4.5.

![Figure 4.5: Bonding technique used for measuring strain and temperature.](image)

A different approach may be found in the literature [155], where the temperature sensor is bonded on one side and put inside a capillary tube, see Figure 4.6. This method is slightly more complicated, but protects the sensor during handling and may even, under certain conditions, be embedded in the material. On the other hand, a sensor bonded in this way should be at the end of a fibre only.

![Figure 4.6: FBG temperature sensor protected by a capillary tube.](image)

In order to reduce the influence of the fibre on the measured result, only the gauge area should be bonded to the plate. It was found during uniform heating experiments in an oven, that the strain measured due to thermal expansion of a plate was unaffected by the bonded extension of the fibre. For this comparison a plate was instrumented according to Figure 4.7.
Since the thermal expansion of the plate was uniform in the tested case, no difference between both sensors was found. However, the same finding is not applicable when strong thermal and strain gradients are present in the area to be measured, since the complete extension of the bonded fibre may influence the measurement in the grating area applying additional strain to the sensor.

4.1.1.3 Comparison of different adhesives

During initial welding experiments, unreliable results were found, such as the strain measurements shown in Figure 4.8. Various reasons for this may exist, but one factor which was further investigated was the use of different adhesives for the application of the sensors. As can be seen later on, the use of more flexible and high temperature resistant adhesives leads to better results.
While all strain results shown in Figure 4.8 have similar tendencies, the repeatability and reliability of these sensors doesn’t seem to be high enough for the expected accuracy of the measurements.

Therefore, using a four point bending experiment, the influence of the adhesive on the room temperature strain measuring properties of FBG sensors bonded by different adhesives was tested. This simplified experiment at constant temperature is made to determine the best adhesive type to be used for strain transmission from the specimen surface to the FBG sensor.

Cyanoacrylate bonded FBG sensors were applied for comparison since these represented the state of the art at the time. For comparison, the epoxy based structural adhesive Araldite 2015 was used with the sensors bonded in all its extension. Finally one pair of sensors was bonded to the specimen in all its extension using Vishay M-Bond 600 high temperature strain gauge adhesive. Figure 4.9 shows the measured strain values at different loading steps during the experiment for all adhesives.

As can be seen, both the strain gauges and the completely bonded sensors using Vishay M-Bond 600 strain gauge adhesive are able to measure tensile and compressive strains in a range of ±1%, which far exceeds the expected welding strain range and are therefore capable of being used in the present work.

Probably due to the higher rigidity, both the epoxy and cyanoacrylate bonded instruments, were not able to measure as high strains as the strain gauge adhesive bonded measuring instruments and should therefore only be used in low strain applications where other properties such as the short curing time of cyanoacrylate based adhesives are more important than accuracy and higher strain measuring capability.
Chapter 4. FBG sensors for welding monitoring

Figure 4.9: Effect of different adhesives on the strain measurement results.

Cyanoacrylate based adhesives have the big advantage of a very short curing time and are therefore interesting for monitoring applications in order to reduce the time necessary for instrumentation. The biggest disadvantage of this type of adhesives however is the low resistance to high temperatures. In order to define the maximum operating temperatures of different types of adhesive, both a high temperature strain gauge adhesive and a cyanoacrylate based adhesive were subjected to temperatures between room temperature and 180°C while being used for measuring strain using FBG sensors. Figure 4.10 shows the thermal expansion strain measured on an aluminium plate when subjected to uniform heating in a laboratory oven.

Figure 4.10: Effect of different adhesives on the strain measurement results; influence of the specimen temperature.
As can be verified, at least for prolonged use and independently of the Bragg grat- ing length, the sensors bonded with cyanoacrylate based adhesive are not capable of measuring strain correctly above 80°C. Even if for very short therm measurements the cyanoacrylate based adhesive may withstand slightly higher temperatures, no measure- ment should be performed in the welding temperature range using this kind of adhesive for strain measurement in order to guarantee a good quality of the results. As can be seen in the same experiment, the high temperature strain gauge adhesive Vishay M-Bond 600 is capable of transmitting the surface strain to the sensing device without problems up to the maximum temperature, at the expense of a more complicated installation procedure and post-curing setup.

4.1.1.4 Accommodation of the sensor

Since the bare fibres are very fragile, the temperature measuring fibres are recoated with acrylate. No significant change of the temperature sensitivity was found. Strain measuring fibres were tested in the bare and recoated conditions. A very low sensitivity for strain was found on the recoated fibres after subjecting those to the necessary post-cure procedure, and therefore strain measuring fibres should be used in the bare state and handled with greater care.

This part of the work discusses the accommodation of sensors in the adhesive both by thermal cycles and mechanical loading. It is important to know this behaviour, since unreliable results can be found in the first heating cycle. During a welding experiment, the first thermal cycle has to be recorded accurately, and therefore it is deemed neces- sary to prevent accommodation of the sensor during the experiment. This should be accomplished before the welding experiment using an adequate post-cure process.

After the normal curing process, the Vishay M-Bond 600 adhesive requires a post curing cycle 30°C above maximum operating temperature for best results. In the current work, post-curing is performed at 200°C. The influence of the fibre coating can be easily seen in Figure 4.11.
Figure 4.11: Static loading of a M(T)80 specimen measured by recoated and bare FBG sensors.

The specimen used for this experiment was a M(T)80 fatigue specimen shown in Figure 4.12. The applied loads where 200N, 11980N and 21760N, and bare and recoated fibres were applied on the back and front side of the specimen. The maximum load is equivalent to a uniform stress of 80MPa in the centre of the specimen, which according to Hooke’s law is equivalent to around 1.2mε. As can be seen, the bare fibres correctly follow the applied load and measure the expected strain amplitude, while the recoated sensors are not able to react correctly to the applied load. This may be due to the degradation of the acrylate based coating of the fibres during the post-curing treatment. It is therefore recommended to use bare fibres for strain measurement whenever temperatures higher than approximately 80°C are reached for best performance and reliable results.

Figure 4.12: M(T)80 specimen used for strain cycling of the FBG sensors.
Post-cure heat treatment and even high temperature curing may not be feasible for large structures because they may not fit in an oven and more importantly because the curing cycle involves prolonged stays above the heat treatment temperature used for the Aluminium alloy chosen for this experiments. AA6082-T6 is solution heat treated at around 170°C which also means that any temperature cycle above 120°C will degrade its mechanical properties due to over ageing.

For this reason, in order to minimise the heat input during the post-curing process of the adhesive, a localised heating procedure for the sensors is developed, based on a hot air gun. In this way localised heating is achieved near the sensor only, reducing the overall influence on the plate. In order to determine the best initial heating for the current application, two maximum temperatures were selected for comparison. First the sensors were heated to only 100°C in order to stay below the limit of 120°C which would lead to over-ageing of the aluminium alloy. In a second attempt, temperatures of 200°C were used. In both cases specimens with two heat cycles were compared to specimens with 4 and 6 such thermal cycles between the ambient temperature and the maximum temperature of 200°C. Each cycle had a very short stage of around 5 seconds at the highest temperature only. Figure 4.13 shows the specimen used for these experiments. Basically it is a 70mm square plate with a thickness of 3mm, where the sensor is applied in the centre.

![Figure 4.13: Specimen used for temperature accommodation experiments.](image)

After being subjected to this installation procedure, thermal cycles were run using a laboratory oven in order to see the sensors’ response to the applied temperature according to Figure 4.14. It can be verified that the sensors had problems during strain acquisition in this case. Figure 4.15(a) shows the raw measurement data, and Figure 4.15(b) shows the treated results.
Both sensor types, the ones heated to 100°C and the ones heated up to 200°C present strong problems in the results. No definitive conclusion should be drawn as to the effect of thermal cycles on the fibres. Anyway, it is suggested as a post-curing step to thermally load the measuring instrument up to 30°C above working temperature in order to eliminate the influences of adhesive accommodation during the measurements. Generally better results were obtained with this procedure as is shown throughout this project.

A similar pre-heating was applied to M(T)80 type specimens, shown in Figure 4.12, up to 200°C only in order to detect the influence of heat cycles when the specimen is afterwards subjected only to mechanical loads at room temperature. Figure 4.16 shows...
the first fatigue cycles augmented in order to be able to detect some influence of the number of pre-heating cycles on the strain measuring performance.

![Graph showing strain cycles at room temperature for sensors with a different number of pre-heating cycles.](image)

**Figure 4.16**: Strain cycles at room temperature for sensors with a different number of pre-heating cycles.

As can be seen, no relation is found between the number of pre-heating cycles using a heat gun and the strain measuring performance at room temperature of the FBG sensors.

Additionally a short fatigue experiment was performed in order to gain some insight into the fatigue performance of FBG sensors. Therefore a M(T)80 specimen was instrumented with two sensors on the front side and two sensors on the back side. Half of the sensors were bonded in bare configuration to the specimen, and the other two sensors were bonded to the specimen after re-coating. Vishay M-Bond 600 adhesive was used in all cases. Fatigue loading was performed for 30000 cycles and static load measurements were taken at 0, 15000 and 30000 cycles at approximately zero load (200N), the maximum load of 21760N and the average load of 11980N. Fatigue loading was performed at 2Hz with the load ratio R=0.1. Figure 4.17 shows the measured average strain for all sensors at different loads. The sensors were subjected to a post-cure at 200°C after instrumentation.

As can be seen from the experiments, bare and recoated strain sensors do have a different response to the applied loads. This may be due to the post curing cycle at 200°C performed during instrumentation. Slight variations of the strain measured along the fatigue life of the specimen were detected, but no explanation is found for this behaviour. It should be noted, that the fatigue specimen apparently did not have any fatigue damages which could be the cause of strain variations. More experiments are needed in order to better define the fatigue properties of both kinds of sensing devices if this behaviour is of interest for a specific topic.
4.1.1.5 Bonding procedure

Unlike for temperature measurement, when measuring strain, the sensors have to capture variations in the length of the sensor due to the deformation of the base material. Therefore the sensing area is bonded in all its extension and under slight tension on the base material. Strain gauge adhesive is used for this purpose, since it guarantees a good transmission of surface strains to the fibre. The steps followed for sensor application are described below.

- Positioning of the sensors with the help of small adhesive stripes. Some tension has to be applied to the fibres.
- The adhesive is applied to the fibre by means of a brush on the sensing part of the fibre only (along 6mm).
- The adhesive is pre-cured in the oven at 24°C for 30 minutes, since without this step the selected adhesive is to fluid.

Figure 4.17: Fatigue experiments on bare and recoated FBG sensors.
Further layers of adhesive are applied to the fibre until the fibre is completely bonded to the surface. Care should be taken to prevent air bubbles and other contaminants in the bonding area.

After 4 hours of curing at 80°C, the instrumentation is finished and the adhesive stripes may be removed.

A post cure of 1h at 30°C over the maximum expected temperature during the experiment should also be performed for best performance and reliable results.

Figure 4.18 shows the application scheme for the strain measuring sensor.

Figure 4.18: FBG strain sensor bonded in all its extent.

It should be noted that these strain measuring fibres also measure temperature, but cannot distinguish between both combined effects by themselves. Therefore it is important that the temperature is measured at the nearest possible point to the strain measuring point, since in a welding process the temperature and strain gradients are considerable. Afterwards the temperature signal may be subtracted from the combined signal in order to obtain only the signal corresponding to strain variations.

4.1.1.6 Bragg wavelength selection

During various experiments it was found that it is not recommended to leave strain and temperature sensors on the same fibre, even if this is theoretically possible. This can be explained by the fact that temperature changes are reflected in a Bragg wavelength shift ten times higher than strain variations, which means that if gratings are inscribed in very similar wavelengths on the same fibre, during experiments where both the temperature and strain change, one signal may interfere with the other and even a switch of the signals may occur and the fibre interrogator unit is not able to distinguish between the peak from the first sensor, for example strain, and from the second sensors, for example temperature, on the same fibre. Even if the various gratings on one fibre measure the same quantity, if the base wavelength of the different sensors is too close, than a small wavelength shift can turn measurements unreliably or even impossible. The interrogator unit may no longer be capable of following the wavelength shift of each of the sensors
inscribed on the same fibre separately. Figure 4.19 explains this problem schematically and later on experimental results show the kind of problems that may arise due to not following the above presented practice.

**Figure 4.19:** Problem which may arise if temperature and strain sensors are inscribed with close central wavelengths on the same fibre.

Keeping in mind Figure 4.19 and the fact that the sensors are wavelength coded, it is easily perceptible that the FBG interrogation unit is not able to know that suddenly in the course of the experiment the temperature sensor is the second instead of the first detected sensor on the fibre. This is the main reason for choosing base wavelengths distant enough between these two kinds of sensors if a single fibre has to be used for some reason. Normally it is more practical to use separate fibres for these two kinds of signals. Various temperature signals may often be used on one fibre and the same holds true for various strain measuring sensors, as long as attention is paid to the initial Bragg wavelength of each of the sensors and the expected wavelength shift during the experiment. Anyway, this fact limits the number of sensors that may be inscribed on each fibre.
Figure 4.20 shows a specimen used in a MIG welding experiment with a high number of sensors.

![Image of MIG welding specimen](image.png)

**Figure 4.20: MIG welding specimen instrumented with a high number of sensors.**

In Figure 4.21 the raw measurement data acquired by the FBG interrogator unit is shown. These results were obtained from a setup where no attention was paid yet to the correct base wavelength of each of the sensors on a single fibre.

As can be seen, even with a very cumbersome repair procedure, the biggest part of the signals is lost due to a wrong selection of base wavelengths for a particular purpose.

In the case described above, signal repair included the elimination of peaks and other erroneous data and the interpolation between known and good data points. While this demonstrated that most signals are repairable up to a certain amount of damage, this approach is definitely not feasible in normal measurement applications.
4.1.2 Interrogator unit

4.1.2.1 Multiplexing capability

Various sensors may be inscribed on each fibre, and various fibres may be connected to the acquisition device. For high speed applications such as welding monitoring it is necessary that the fibres can be read simultaneously, or at least multiplexed at a higher rate than the desired acquisition rate. Figure 4.22 shows the measurement of one strain and one temperature sensor with an equipment without the capability to multiplex the channels at a sufficient high rate.

As can be seen, both signals are not recorded simultaneously. This has two major drawbacks. First the information is not complete for both sensors. Secondly, since for the strain measurement the temperature measuring fibre has to be subtracted from the temperature and strain measuring fibre, it is mandatory that both signals exist simultaneously. In steady state of very slow phenomena, signals may be interpolated.
between known signal values, but for the fast welding process this is not valid anymore (at least for the acquisition rate shown in Figure 4.22).

### 4.1.2.2 Acquisition rate

In order to define a simple curve, around ten data points should be sampled for each round part of a curve in order to define it sufficiently. Welding is a fast process which leads to strong temperature and strain variations and gradients. Figure 4.23 illustrates what happens if the acquisition rate is not high enough.

**Figure 4.23:** Strain and temperature recorded by FBG sensors during a MIG welding experiment; the acquisition rate was 1 Hz.
As can be seen, only a few points are recorded in the interesting part of the experiment. While the initial part of the experiment with static temperature is sufficiently well recorded, the fast heating and cooling parts of the process are not recorded correctly. 1Hz was used for the acquisition rate in this case due to limitations in the used equipment.

Other problems also occurred with the instrumentation in this experiment which may explain the high peak magnitudes. Anyway, it is shown that a high acquisition rate is necessary, even if the cost for the necessary equipment is higher.

## 4.2 Fibre Bragg Grating sensor calibration

Before any kind of measurement can be performed in a welding process, the measuring devices have to be calibrated, so that the obtained results may be considered valid.

For each combination of base material, adhesive, coating and fibre, a calibration curve has to be recorded which describes the shift in the Bragg wave length due to the temperature variation, so that the results measured are comparable to results measured by strain gauges.

For constant temperature strain calibration, a four point bending test is proposed. Both tensile and compressive strains may be calibrated up to about $\pm 10\mu\varepsilon$. For temperature calibration a industrial oven capable of reaching temperatures between $-70^\circ C$ and $200^\circ C$ is used.

For strain calibration a further method was developed, which is based on the comparison of the measured strain and the calculated thermal expansion of a plate when subjected to temperature changes. Only tensile strains were calibrated using this methodology.

Therefore, calibration of FBG sensors for measuring strain and temperature on aluminium plates using strain gauge adhesive is performed before the actual experiments corresponding to the intended application.

### 4.2.1 Calibration for strain measurement using the four point bending test

The calibration for strain measurement at constant temperature is performed using the four point bending technique. This type of mechanical test gives adequate space for installation of sensors, and minimises the strain gradient in the instrumented region, since the bending moment is constant between inner rollers, and in the linear elastic regime strain in the upper and lower surfaces is proportional to this moment [233]. If
continued above the elastic limit, this type of test makes it possible to measure a large range of tensile and compressive strain values.

Since both sensors, electrical strain gauges and FBG sensors, measure average values of strain along their gauge lengths, it is important to know the strain distribution within the measured region, and FEM results can provide a clarification of that question [234]. Surface strain measurements using electrical strain gauges were used for the calibration of the FBG sensors.

### 4.2.1.1 Experimental setup

The specimen is a 180mm long beam of rectangular 20×3mm cross section; in the case of the calibration of 5mm long gratings, the cross-section was 45×3mm. The specimen is loaded by rollers of diameter 30mm on the upper surface and 20mm on the lower surface. Figure 4.24 presents the experimental setup. For calibration of the 10mm long gratings, the centre of the specimen was instrumented in the upper and lower surfaces with 10mm long FBGs and Vishay SA-13-250BG-120 strain gauges with a gauge length of 6.35mm (0.25”). For the calibration of 5mm long gratings, Vishay CEA-13-125UN-120 strain gauges with a gauge length of 3.18mm were used. The specimen bending was carried out on a servo-hydraulic MTS machine equipped with a 250kN load cell at constant room temperature under displacement control. The actuator speed was 1mm/min. After plasticity occurred the movement of the actuator was inverted, and the test finished when the specimen was no longer in contact with the actuator.

![Figure 4.24: Experimental setup for the four point bending test.](image-url)
4.2.1.2 FEM model

During elastic deformation of a specimen in the four point bending test, the strain between the inner rollers on the top and bottom surfaces is constant, and therefore the sensing length of the measuring device does not influence the results. Due to the plasticity reached during the calibration procedure, however, strain is not constant anymore, and therefore a FEM model considering geometrical and material non-linearity was solved using Abaqus [235] in order to determine the magnitude of the possible error introduced by calibrating 5 and 10mm long fibres using 6.25mm long strain gauges. Plane strain condition was assumed and yielding of the material and contact effects were included in the model. The rollers used were modelled as rigid cylinders. Rigid body motion was prevented by constraining the central line of the specimen in X direction and reaction forces were negligible. Figure 4.25 shows the distribution of the strain between the inner rollers for maximum load.

![Figure 4.25: Detail of $\varepsilon_{xx}$ strain distribution at maximum load.](image-url)

Experimental validation was performed by measuring the force and displacement of the actuator during the test. After the test, the plastic deflection of the centre of the specimen was also measured. Good comparability was found, suggesting that the finite element model correctly represented the experiment.

In the linear elastic regime strain in the upper and lower surfaces is constant in the region between inner rollers, and therefore no influence of the sensor length (6.35mm in the case of electrical strain gauges and 5 and 10mm in the case of the FBG sensors) is to be expected. A study was carried out to evaluate this question in the elasto-plastic regime, and again it was concluded that the length of the sensor does not significantly influence results. The finite element model shows that even for a substantial plastic deformation, strain in the measured region is approximately constant. The maximum difference between strain results for the centre ($\varepsilon_1$) and the extremity of the sensing length of 10mm ($\varepsilon_2$) shown in Figure 4.25(b) is 0.47%, in the case of maximum plastic deformation. The obtained results are therefore valid for the different gauge lengths.
4.2.1.3 Results

Strain was measured on top and bottom of the specimen by using electrical strain gauges and FBG sensors. Figure 4.26 shows the Bragg wavelength shift $\Delta \lambda_B$ as a function of the strain measured by electrical strain gauges.

![Graph showing Bragg wavelength shift vs strain for FBG sensors bonded using Vishay M-Bond 600 strain gauge adhesive.](image)

**Figure 4.26:** Strain calibration curves for FBG sensors bonded using Vishay M-Bond 600 strain gauge adhesive.

The strain measured by both devices on the surfaces of the specimen is similar to the FEM results. For small displacements the behaviour of the specimen is linear elastic, but progressively diverges from the linear elastic model. It should be noted that both sensor types (electrical strain gauges and FBG sensors) behaved adequately during the experiments. The calibration of the FBG sensors was based on the strain gauge results. The relation between applied strain and measured Bragg wavelength shift is $0.0012 \text{ nm/\mu e}$ for the 5mm long gratings and $0.0013 \text{ nm/\mu e}$ for the 10mm long gratings and can therefore be considered equal in engineering therms.

4.2.2 Calibration for strain measurement using thermal stresses

Strain calibration was also performed using a laboratory oven. In this case, the theoretical thermal expansion of the specimen is used for calibration of the values measured by the FBG sensors. Four 5mm long bare gratings and three 10mm long gratings are used in the first set of calibration experiments. Four 5mm long bare gratings are used in the second set of calibration experiments. The experimental setup was similar in both cases, only the applied thermal cycles were different.
4.2.2.1 Experimental setup

Figure 4.27 shows the experimental setup for the temperature and strain calibration procedure using a laboratory oven and heat induced stresses.

\[ \varepsilon = \alpha \times \Delta T \]  

Figure 4.27: Experimental setup for FBG calibration using a laboratory oven.

In the first set of experiments only one thermal cycle is applied, and the calibration values are taken in 20\(^\circ\)C steps from ambient temperature up to 180\(^\circ\)C.

Since this is not the best way of performing calibrations, in the second set of calibration experiments, the specimen is heated to 200\(^\circ\)C in steps of 20\(^\circ\)C and cooled down in the same steps in order to also use the cool down phase of the thermal cycle for calibration. The acquired data is used for calibrating the strain readings by the calculated thermal expansion of the specimen for that specific temperature variation, see equation 4.1.

Before the first measurement, a post-cure cycle is made, keeping the specimen at 200\(^\circ\)C, the highest possible temperature of the available oven, for 1h.
Figure 4.28 shows the specimen and the used instrumentation scheme. The connection sequence of the sensors to the measurement equipment is marked on this drawing.

![Figure 4.28: Specimen drawing with instrumentation scheme.](image)

### 4.2.2.2 Results

Figure 4.29 shows the obtained calibration results for the both sets of experiments, including strain calibration results for 5 and 10mm long FBG sensors.
Chapter 4. FBG sensors for welding monitoring

The calibration factors obtained from this experiment are 0.0012 nm/µε for the relation between strain and the corresponding Bragg wavelength shifts for both, the 5mm and 10mm long FBG sensors. As can be verified, both sets of tests led to the same calibration constants. Additionally, this calibration procedure for strain using thermal stresses leads to very similar results to the mechanically strained specimens’ results shown before.

4.2.3 Calibration for temperature measurements

The FBG sensors were calibrated using type-K thermocouples in a temperature controlled oven. It is relatively easy to isolate the thermal effects from strain effects by the used application technique which guarantees that no strain is transferred to the fibre, similar to dummy gauges in electrical strain gauge measurements.

4.2.3.1 Experimental setup

Temperature measurement calibration for FBG sensors was performed in an oven capable of reaching temperatures between -70°C and 200°C which on the one side is enough for the intended range to be measured during the welding experiments, and on the other side is very near the maximum temperature which the adhesive used for sensor application can withstand.

Figure 4.30 shows the specimen used for calibration purposes. The same aluminium and bonding technique as in the welding experiments were used for accurate results.

Figure 4.29: Strain calibration results.
4.2.3.2 Results

Figure 4.31 shows the temperature calibration curve obtained for fibres with 5 and 10mm long gratings. The calibration results for the 5mm long gratings include results from different fibre fabrication dates, and therefore small differences are recognisable between the first four and the last two results for the 5mm long gratings.

![Graph showing temperature calibration curves for FBG sensors bonded using Vishay M-Bond 600 strain gauge adhesive.](image)

Figure 4.30: Specimen used for temperature calibration of FBG sensors.

Figure 4.31: Temperature calibration curves for FBG sensors bonded using Vishay M-Bond 600 strain gauge adhesive.
A nearly linear relationship between Bragg wavelength shift and temperature is found, and the difference between 5 and 10mm long gratings is negligible. The calibration factor obtained from this experiment is \(0.01 \frac{\text{nm}}{\text{°C}}\) for the relation between temperature and Bragg wavelength shift for both, the 5mm and 10mm long FBG sensors.

4.3 Welding monitoring

4.3.1 Welding process selection

MIG welding was chosen because of two main reasons. It is an easily accessible process, capable of welding the studied aluminium alloy in a robotic configuration, and it permits testing the sensors in harsh environments, which are less severe in most industrial welding processes of interest for this measuring technique.

A second experiment was then performed on FSW due to the high interest of understanding this welding process and of overcoming the shortcomings of other monitoring techniques in this process such as the Digital Image Correlation (DIC) based techniques, due to the reduced available space because of the strong clamping requirements.

For measurement using the FBG based optical sensors, the calibration factors determined during the corresponding experiments were used. Obtained results concern the weld bead side of the plate, in the case of MIG, and the shoulder side in the case of FSW.

Equations 4.2 to 4.3 are therefore used for transforming the measured Bragg wavelength shift into temperature and strain data. The initial temperature is measured by the thermocouples.

\[
T = \frac{\Delta \lambda B_t}{0.01} + T_{initial} \quad (4.2)
\]

\[
S_{\text{corrected}} = \frac{\Delta \lambda B_{ts} - \Delta \lambda B_t}{-0.0012} - \alpha \Delta T \quad (4.3)
\]

\[
S_{\text{uncorrected}} = \frac{\Delta \lambda B_{ts} - \Delta \lambda B_t}{-0.0012} \quad (4.4)
\]

\(\Delta \lambda B_t\) is the Bragg wavelength shift due to temperature and \(\Delta \lambda B_{ts}\) is the Bragg wavelength shift due to mechanical strain and temperature applied to the sensor. \(\alpha\) is the thermal expansion coefficient of the material to be measured.
4.3.2 Instrumentation

4.3.2.1 MIG experimental setup

While the FBG sensors were shown to work when installed on the lower surface of the plate by Suarez et al. [155], in this work the less protected upper surface, where the welding radiations are more apparent, was instrumented. MIG welding was performed on a $360 \times 250 \text{mm}^2$ wide, and 3mm thick plate of the aluminium alloy AA6082 in the T6 condition. Figure 4.32 shows the instrumentation scheme used in this experiment, and Figure 4.33 shows a picture of the instrumented specimen.

![Figure 4.32: Instrumentation scheme for the MIG welding experiment.](image)
Four strain gauges and four thermocouples were applied, and the results were compared to four temperature measuring fibres and four strain measuring fibres. All fibres were protected with a silicon based paste, and strain gauges were protected with a dedicated strain gauge protective coating, Vishay 3145 RTV. Thermocouples and temperature measuring FBG sensors were covered with silver based thermal compound for better heat transfer before the silicon based protective cover was applied as used by Moreira et al. [156].

Vishay M-Bond 600 was used for bonding the strain measuring fibres and the strain gauges. The specimen was subjected to a adhesive post-cure for 2h at 200°C. This additional procedure introduces some problems for real world applications, since the instrumentation process takes longer, and the prolonged exposure to high temperatures does significantly affect the heat treatment of AA6082.

### 4.3.2.2 FSW experimental setup

Friction stir welding was performed on a 360×250mm² and 3mm thick AA6082-T6 plate. Figures 4.34 and 4.35 show the instrumentation schemes used in this experiment, and Figures 4.36 show pictures of the instrumented specimen for both schemes. The difference in both schemes is the location of the strain gauge in relation to the FBG sensors, which leads to an easier installation in the second scheme.
Figure 4.34: First instrumentation scheme for the FSW welding experiment.

Figure 4.35: Second instrumentation scheme for the FSW welding experiment.
Vishay M-Bond 600 was used for bonding the strain measuring fibres and the strain gauges to the plate. Due to the special bonding technique used and the very small diameter of the optical fibres, temperature and strain may be measured almost on the same location. The plate was subjected to a post-cure for the adhesive for 2h at 200°C in an oven. This additional procedure introduces some problems, since the instrumentation process takes longer, and the heat stage can significantly affect the heat treatment of the aluminium alloy AA6082-T6.

The silver based thermal compound “Artic Silver 5” was used in order to guarantee a better heat transfer from the specimen to the sensors on plate B only. Measurement was performed at 200Hz with both kinds of sensors.

### 4.3.2.3 Welding parameters

The MIG welding was performed with the parameters described in Table 4.1.

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
</thead>
<tbody>
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<td>Welding speed</td>
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</tr>
<tr>
<td>Stick-out</td>
<td>20mm</td>
</tr>
<tr>
<td>Current</td>
<td>128A</td>
</tr>
<tr>
<td>Arc voltage drop</td>
<td>17.1V</td>
</tr>
</tbody>
</table>

FSW was performed in the workshop of Departamento de Engenharia Mecânica (DE-Mec) under displacement control on a Induma universal milling machine with the parameters presented in Table 4.2.
Table 4.2: FSW welding parameters.

<table>
<thead>
<tr>
<th>parameter</th>
<th>value</th>
</tr>
</thead>
<tbody>
<tr>
<td>Welding speed</td>
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</tr>
<tr>
<td>Rotation speed</td>
<td>1500rpm</td>
</tr>
<tr>
<td>Plate thickness</td>
<td>3.02mm</td>
</tr>
<tr>
<td>Penetration</td>
<td>2.97mm</td>
</tr>
<tr>
<td>Shoulder diameter</td>
<td>15mm</td>
</tr>
<tr>
<td>Pin</td>
<td>Conical pin with tears, φ = 5mm</td>
</tr>
<tr>
<td>Welding type</td>
<td>deposition</td>
</tr>
</tbody>
</table>

4.3.3 MIG

The first experiments were performed using the MIG welding technique since this process is very aggressive when compared to most other industrial welding techniques. This means that if the FBG sensors are capable of measuring correctly in this harsh conditions, measurements on solid-state processes such as friction stir welding should be flawless.

4.3.3.1 Results

Figure 4.37 shows a comparison of the specimen before and after the welding experiment. The harshness of the process can be recognised, and the distortion of the plate can easily be seen.

![Figure 4.37: Specimen before and after the MIG weld.](image)

Figures 4.38 show the plots of the strains and temperatures measured by traditional electrical sensors and by FBG based optical sensors.
While the temperature profiles seem quite similar between electrical and optical sensors, the strain profiles are different. In this context it should be remembered that, while the electrical strain gauges are self temperature compensated, the FBG sensors are not, and therefore measure the thermal expansion of the specimen additionally to the mechanically induced strain, which is considerable in a welded specimen. Self temperature compensation is achieved by selecting a strain gauge grid material which has the same thermal expansion coefficient as the specimen material itself, which means that the strain gauge will expand with the specimen without affecting the resistance of the grid significantly when only thermal loads are applied.

One possibility to compare the results considering this effect is to look only at the residual strain value, which is quite similar in the present case after the plate cooled down, see Figure 4.38(c) and 4.38(d) at the end of the experiment. Another approach is shown in Figure 4.39 where the theoretical thermal expansion of the aluminium measured by the thermocouple is added to the mechanical strain measured by the self temperature compensated strain gauge.
This approach leads to very comparable results between both measuring techniques. It is based on the assumption that since strain gauges physically subtract the thermal expansion of the substrate material, this value may be added for comparison purposes. In this way it is possible to obtain a good approximation of the full strain and not only the mechanical part during the welding process. One problem to be addressed is the fact that the thermocouple and strain gauge are not exactly at the same place on the plate, which for high thermal gradients as they happen in a MIG weld may introduce some errors in this approach, and that all measurements are superficial. In the present case, since the thermocouples are placed approximately 5mm before the centre of the strain gauges in welding direction, the thermal expansion has an offset in negative temporal direction when compared to the FBG sensors, which can explain why in Figure 4.39(a) the strain seems to start rising earlier than in Figure 4.39(b) in relation to the temperature curves.

4.3.4 FSW

Various FSW joints were performed, leading to deep knowledge regarding the related instrumentation problems and requirements. The results shown in this section include the knowledge acquired during the other experiments and can therefore be seen as final results.

Measurements were started after clamping and stopped only after clamping release. Both measuring instrument types, strain gauges and FBG sensors, may be compared in this way. During welding, the ambient temperature was 26°C.
4.3.4.1 Results

Figures 4.40 show the plots of all strains and temperatures measured by traditional strain gauges and thermocouples compared to FBG sensors.

![Figure 4.40: Strain and temperature measured during the FSW test.](image)

The difference obtained between strain sensors based on optical fibres and electrical sensors is severe in the region of high thermal loads. Adding the thermal expansion to the measured strain by electrical strain gauges, a better comparison to the total strain measured by FBG sensors is possible. Figures 4.41 and 4.42 show these results.
In a further experiment, a slightly different instrumentation setup was chosen in order to optimise the instrumentation procedure.

As in the case shown before, the FBG based results were corrected in order to not include the plate expansion only due to thermal effects, similar to the self temperature compensation of the electrical strain gauges for comparison reasons only.

Figure 4.43 shows the measured temperatures on all points using both kinds of sensors.
As can be seen, the sensors symmetric to the welding line on plate B had a more synchronous response than the same sensors on plate A. This was due to the use of a thermal compound for optimising the heat transfer to the sensors on plate B. The importance of using such compounds for sensor installation is therefore confirmed.

Figure 4.44 shows the measured mechanical strain on all points using both kinds of sensors.
The correction for the exclusion of the thermal expansion effect for both kinds of sensors seems to work, but different strain readings are obtained after the minimum peak on both sensors. The corrected FBG based sensors have differences in relation to the strain gauges, since the temperature is not measured exactly on the intended strain measurement point, but with a small offset, which is enough for introducing these differences. For example the sensors “FBG 4” and “FBG 6” on plate A were positioned very near the strain sensor. This procedure leads to a good correction for thermal effects, but on the other side the temperature measurements are noisy due to their contact with the adhesive from the strain sensor.

FBG sensors measure both the mechanical and the thermal strain applied to them. Self temperature compensated strain gauges do not measure the thermal strain. If a direct comparison is to be made between these two kinds of sensors, the thermal expansion of the aluminium plate has to be subtracted from the FBG readings or added to the strain gauge readings. This only works well, if the temperature is measured at the same location...
of the strain, since any small offset between those sensors will lead to wrong corrections of the total strain due to the high thermal gradients in welding applications. Therefore the corrected strain values presented before should be considered as verification values only, which indeed show that FBG sensors and strain gauges measure equivalent values, but at the end only the uncorrected total strain readings should be presented as a result.

Figure 4.45 shows the total strain measured by the FBG sensors in both experiments. These measurements include the thermal and mechanical strain introduced by the FSW process.

As can be seen from Figure 4.45(b), sensors “FBG 1” and “FBG 3”, one problem with using multiple sensors on one fibre is that they are connected in series, which means that errors in one sensor may affect all the other sensors on the same fibre. Nevertheless it may be seen that FBG sensors are very well capable of measuring a systems total surface strain even if is is subjected to high temperature variations.

One interesting feature of these curves is the residual surface strain. At the end of the recording of Figure 4.45, the temperature was around 35°C as can be seen in Figure 4.43. Therefore only a little strain decrease is expected after cooling to room temperature. At the end of the measurements, on plate A, 500µε were recorded for all points, and on plate B with some more measurement problems, values around 700µε, with exception of one very low value, were recorded at a distance of approximately 50mm from the welding line centre. Translating these strains into stresses by using Hooks law, a surface residual stress near 35MPa for plate A, and near 47MPa for plate B was found. Comparing these values to results obtained by the Incremental hole drilling technique (iHDT) at a distance of 49mm from the welding line centre on a comparable plate [236], a very
good agreement with the near-surface residual stresses could be found, see residual stresses in Y direction in Figure 4.46. This Figure shows additional information to data already published [236]. In the present case only the stresses in Y direction are of interest for comparison. Measurements were performed according to ASTM E837-07 [113]. It should be remembered that both, the FBG measurement technique and the iHDT technique are not error free, and therefore such a good agreement may not always be true. Furthermore the iHDT has difficulties in providing accurate measurement results very near the surface, see Section 6.3.3. Anyway, it can clearly be seen that the residual strain measured by the FBG sensors provides a good indication of the surface residual stress present at the measurement location. This can obviously only hold true if the initial residual stress, present before FBG sensor installation, was negligible.

![Graph showing X and Y stresses](image)

**Figure 4.46:** Residual stress measured using the iHDT at a comparable location as the surface FBG measurement results.

### 4.4 Comparison of strain and temperature for both welding processes

Both welding processes may be compared in a variety of ways. While the base material is the same in both cases and the final result is a defect free welded plate, the welding speed of the MIG welding process was significantly higher. Nevertheless, a comparison of the main strains and temperatures between both welding processes based on the FBG sensor measurements can be made. Measurements were made at comparable sites on the plate, at the same distance to the welding line (50mm). When comparing the temperatures in the MIG weld, see Figure 4.39(b), and FSW, see Figure 4.41(b), it can be seen that at a
distance of 50mm to the weld, the measured peak temperatures in the fusion process are approximately 70% higher than in FSW. Additionally, the time from peak temperature to a temperature below 40°C is around 40 seconds in FSW, while for the MIG welding process it takes almost 10 times as long, which means that significantly more heat was introduced by the MIG welding process. In terms of strain, it can be seen from Figures 4.39(b) for MIG and Figures 4.41(b) for FSW, that the residual strain, which is the strain left in the plate after cooling down, is between two and three times higher in MIG than in the FSW process. This justifies the lower distortion found in the FSW plate. During the welding processes, in the case of MIG welding, the tensile strains measured with strain gauges, which do not include the thermal expansion of the material, are also higher than in the FSW process, while the compressive strains are almost identical. The compression in the case of FSW most likely is originated by the lateral forces created by the tool which penetrates the material and therefore partially displaces it to the sides compressing the clamped material. After the tool passes, the strains turn into low tensile strains, eventually reaching almost zero after the clamping is released. In the case of the MIG welding process, tensile strains appear in the cooling down phase due to the contraction of the added material in the centre. The compression seen when the torch passes the strain gauges is due to the higher volume of the fused addition and parent material which compresses the restrained plate outside of the bath area where the strain gauges are placed. Looking at the results measured by FBG sensors, which do include both the specimen mechanical strain and the thermal strain, significantly higher total strains are measured during the MIG welding process, which are related to the higher thermal energy introduced by the MIG welding process. This high stress together with the high gradient expected in this fusion welding process is most likely the main responsible for the distortion observed in the plates, and should therefore be monitored in a variety of conditions, using simple and cost effective instrumentation systems such as FBG sensors. The multiplexing capabilities of the FBG sensors make it possible to acquire even more data points with a single fibre.

4.5 Conclusions

In this work the use of FBG sensors was demonstrated for harsh traditional and modern welding processes obtaining good results. Before the welding experiment, the sensors were calibrated in order to obtain valid results and various experiments were performed in order to determine the best setup and instrumentation procedures.

The use of the four point bending test to calibrate FBG sensors along a large range of positive and negative strain values was presented and compared to calibration results
based on the thermal expansion of the specimen. Measurements of strain gauges were used for calibration of the mechanical strain measuring FBG sensors. The calibration curves for FBG sensors with gauge lengths of 5 and 10mm measuring strain between $-10\,\mu$ε and $10\,\mu$ε were determined.

Temperature was successfully calibrated using an oven, taking into account the material and adhesive later used in the welding experiment. The calibration curve for FBG sensors measuring temperature between 30°C and 180°C was obtained successfully.

With the knowledge acquired, MIG welds were performed and strain and temperature was successfully measured by strain gauges, thermocouples and FBG sensors, demonstrating the ability of the FBG sensors to withstand harsh environmental conditions while leading to trustworthy results. The same instrumentation was then applied to FSW, which is a modern solid state joining process, and good results were obtained. High repeatability of both the welding processes and of the instrumentation used for monitoring them could be observed. As shown throughout this project, FBG sensors are capable of being used for welding monitoring applications provided that the instrumentation is done according to the developed scheme. It should however be noted that FBG sensors, contrary to strain gauges, but similarly to other optical surface strain measurement techniques, do measure the complete strain applied to the workpiece. This includes both the thermal expansion of the workpiece and the mechanical strain applied to the system.

One challenge with this kind of sensors is the inability to repair sensors unlike what may be done under certain circumstances on electrical sensors. Cables may be re-soldered to a strain gauge, while the fibres used for FBG sensors may be very difficult to repair in situ when they brake near the bonded sensing area. The equipment used for joining fibres is too big, smaller tools have therefore to be developed.

The sensors ability to withstand harsh welding conditions while acquiring signals, paves the way for structural health monitoring based on FBG sensors, which may be installed on the components surface and monitor their lives from the beginning, through the production process until the end of life of a structure. It was successfully shown that the surface residual strains are equivalent to the surface residual stresses measured by the iHDT, which paves the way to such an approach. Such an instrumentation would allow to have a good knowledge of the actual stress state inside, or at least at the surface of a structure, which would allow to greatly improve the maintenance planing.
Chapter 5

Clamping force and Friction Stir Welding

FSW is a novel solid state joining process of high interest for the aeronautical industry. In general, low residual stresses and distortions are mentioned as advantages of this process, due to its solid state nature.

In order to achieve these beneficial properties, the welding process requires rigid clamping of the parts to be welded. To the author’s knowledge clamping forces in FSW have not been thoroughly studied in detail yet, neither their influence on residual stresses and distortion.

In the present work the clamping forces, both initially applied and their evolution during the welding process, were studied. Furthermore the influence of different clamping forces on the distortion and residual stress state of welded plates was analysed. The welds were characterised mechanically and metallurgically in order to guarantee defect free joints in all cases.

It was found that higher clamping forces lead to lower distortion and a more uniform residual stress distribution through the thickness, but at the cost of a higher average residual stress. Higher clamping forces also lead to a lower defect probability through gap generation between the plate halves.
5.1 Equipment

5.1.1 Clamping device

For a sound FSW joint each component must be positioned and held securely in place during the welding process. In order to be able to guarantee constant quality, also the clamping process should be repeatable.

A clamping device was developed by the author, which is able to control the initial clamping force and measure the evolution of the clamping force during welding. The obtained data may be used to design better clamping devices and to provide further input for numerical simulations of the friction stir welding process. Furthermore, insight is gained on the influence of the clamping system on distortion and the residual stress distribution.

The developed clamping device is based on four instrumented beams for the vertical load control and four resistive load cells for the horizontal load control, see Figure 5.1. Details about this system can be found in Appendix D.

![Clamping force measuring device prepared for welding.](image)

**Figure 5.1:** Clamping force measuring device prepared for welding.

5.1.2 Welding

A Neos Tricept TR805 robot with a custom force controlled FSW head was used. The welding parameters, equal for all welds, are shown in Table 5.1. The three-flat welding pin is shown in Figure 5.2.
Table 5.1: Nominal welding parameters.

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<tr>
<td>pin length</td>
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</tr>
<tr>
<td>downforce</td>
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</tr>
<tr>
<td>advancing speed</td>
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<tr>
<td>rotational speed</td>
<td>600rpm</td>
</tr>
<tr>
<td>pin</td>
<td>three-flat, ( \phi = 4)mm</td>
</tr>
<tr>
<td>shoulder</td>
<td>striated, ( \phi = 17)mm</td>
</tr>
</tbody>
</table>

Figure 5.2: Pin and shoulder used for welding.

Figure 5.3 shows the readout of the robots internal data acquisition system for rotational speed and vertical force which was used for force controlled FSW as an example. The welding force and rotational speed on all welds was similar.

Figure 5.3: Welding force and speed.
The process forces were also measured by four 3D Kistler load cells mounted beneath the welding table. Figures 5.4 shows the process forces acting during welding when the initial clamping forces in both directions were set to zero and Figures 5.5 show the process forces acting during welding when the initial clamping forces were set to the maximum of 2500N in both directions. As can be verified, no significant difference can be detected in both cases considering the expected measurement errors for this kind of devices.

![Figure 5.4](image1.png)
(a) Z direction

![Figure 5.4](image2.png)
(b) X and Y directions and torque around Z

**Figure 5.4:** Process forces acting during welding. Clamping forces 0N; 0N.

![Figure 5.5](image3.png)
(a) Z direction

![Figure 5.5](image4.png)
(b) X and Y directions and torque around Z

**Figure 5.5:** Process forces acting during welding. Clamping forces: 2500N; 2500N.

This means that the welding forces are comparable for all cases in this work. Different distortion and residual stresses therefore result from changes in the clamping conditions only.
5.1.3 Distortion measurement system

The shape distortion was measured by the optical Pontos system from Gesellschaft für Optische Messtechnik (GOM mbH). This stereo camera based system acquires the three dimensional location of markers bonded to the workpiece surface. Schneider-Kreuznach lenses with a focal length of 50mm and a maximum aperture of f2.8 were used with an aperture of f8 to guarantee a sharp image throughout the whole measurement volume. The specimens were illuminated by a Light Emitting Diode (LED) based lighting system developed for such measurement tasks. During calibration, a pixel deviation well below the limit of 0.04 pixels defined by the manufacturer was achieved for all specimens.

5.2 Clamping force

5.2.1 Initially applied clamping force

The first goal of the present work was to control the initial clamping conditions of all specimens. 39 specimens were welded in the same conditions, changing only the clamping forces and the initial gap between the plates. Most of the process parameters were monitored during the weldings in order to ensure a good comparability between experiments. Monitored objects included the clamping force before and during the welding process, temperature at 10 points using thermocouples, process forces, both measured by the robot and an external Kistler 3D load cell, shape distortion using a stereo vision based deformation measurement system, and the welding parameters controlled by the welding head and robot. The initial clamping forces and the initially introduced gap between plates can be seen in Table 5.2. The gap was initially introduced by squeezing spacers with the desired thickness and of the same material in-between both plate halves.
Table 5.2: Specimen description and performed characterisation. [Mi - Microhardness measurement; BT - Bending test; Ma - Macrographic analysis; TT - Tensile test; RS\textsubscript{S} - Synchrotron XRD residual stress measurement; \textit{RS}_{C} - Contour method residual stress measurement; FCG - Fatigue crack growth test using C(T) specimens].

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Table 5.3 shows the number of specimens welded at each clamping force combination between the established limits. Some of the specimens were also welded with a well defined initial gap.

Table 5.3: Initial clamping force matrix.

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<td>2500</td>
<td>2</td>
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5.2.2 Clamping force evolution during FSW

Figure 5.6 compares the evolution of the clamping forces for two different cases. In the case of Figures 5.6(a) and 5.6(c), the initial clamping force was approximately 0N in both axes, and in the case of Figures 5.6(b) and 5.6(d), the maximum clamping force of 2500N was applied with each clamp.

Process forces and temperature measurements during both weldings did not reveal any significant difference between both cases. Differences were however found in the final shape distortion and the gap between both plates after welding. The higher clamping forces led to an overall distortion of 11.8mm, while the low clamping forces led to an overall distortion of 14.5mm according to Section 2.4.2.

Both weldings were started without initial gap between both butt-welded plates. While the heavily clamped specimen retained its 0mm gap, the loosely clamped specimen had a gap of approximately 0.7mm at the start of the weld, where the pin penetrates the plates and forces them to the sides, and 0.1mm at the end of the plate. This may influence the mechanical behaviour and quality of the weld, especially at the start. After both plates were initially welded, the tendency of separation of the plate halves was reduced.
As can be seen from Figure 5.6, the horizontal clamping force reaches a maximum of approximately 4000N at the start of the weld on the retreating side for both cases. Due to the plate distortion, the final horizontal clamping force is significantly reduced, while the final vertical clamping force rises slightly. One noticeable difference between both specimens, is that in the strongly clamped one, the load cells at the end of the weld feel the forces introduced by the pin later than in the loosely clamped specimen.

### 5.3 Influence of the clamping force on mechanical properties

Base material characterisation of AA2198-T851 was performed in order to be able to define the welding efficiency achieved by FSW. It should be noted that the temperature evolution variation between specimens was small and did not depend on the applied
initial clamping forces, see Figures 5.7. The detected variations may possibly be related with very small variations in the positioning of the thermocouples. Since high thermal gradients are measured, very small position variations may lead to relatively high temperature variations. Temperature was measured using ten thermocouples at different positions along the welding line at a distance of 9mm to the welding centre.

![Graphs showing temperature variations](image)

(a) Approximate initial clamping force: 2500N  
(b) Approximate initial clamping force: 0N

**Figure 5.7:** Temperature acting during welding.

A mechanical and metallurgical characterisation was performed on selected specimens, including tensile tests, microhardness measurements, bending tests and a metallographic analysis of the joint. Since it was shown that a loosely clamped plate leads to a higher gap between the plates, mechanical properties may be affected. The joint characterisation shown in Figure 5.8(a) of friction stir welded AA2198-T851 was performed with a gauge length of 25mm using a MTS clip gauge, model 634.12F-54 and the joint characterisation shown in Figure 5.8(b) was performed using a laser probe with a gauge length of approximately 30mm.

Contrary to the base material a relatively high variation between different specimens was found. Especially the elongation after break of the loosely clamped samples near the start of the weld was affected. Table 5.4 shows the average tensile test results depending on the clamping force applied to the specimens and the gap between the plate halves measured at the start and end of the weld after welding. The yield strength was determined for a proportionality limit of 0.2%.

As can be seen, a very low horizontal clamping force seems to lead to slightly higher gaps between the two plate halves. On the other hand there doesn’t seem to be a significant influence of the clamping force on the average tensile test results. Anyway, as can be verified in Figure 5.8, the higher clamping forces tend to create more uniform welds with more constant tensile properties along the joint. This may be related to the gaps
(a) Approximate initial clamping force in horizontal and vertical directions: 0N
(b) Approximate initial clamping force in horizontal and vertical directions: 2500N

**Figure 5.8:** Tensile tests performed on AA2198-T8 joints with different clamping conditions.

**Table 5.4:** Influence of the clamping conditions on tensile properties and gap.

<table>
<thead>
<tr>
<th>Clamping force</th>
<th>gap start</th>
<th>gap end</th>
<th>E [GPa]</th>
<th>σ_y [MPa]</th>
<th>σ_r [MPa]</th>
<th>ε [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Base material</td>
<td>-</td>
<td>-</td>
<td>71</td>
<td>450</td>
<td>499</td>
<td>15</td>
</tr>
<tr>
<td>2500N; 2500N</td>
<td>0.1</td>
<td>0</td>
<td>72</td>
<td>264</td>
<td>380</td>
<td>9</td>
</tr>
<tr>
<td>2500N; 1500N</td>
<td>0.1</td>
<td>0.2</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2500N; 500N</td>
<td>0</td>
<td>0.2</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>2500N; 0N</td>
<td>0.5</td>
<td>0.4</td>
<td>67</td>
<td>275</td>
<td>383</td>
<td>8</td>
</tr>
<tr>
<td>2000N; 500N</td>
<td>0.5</td>
<td>0.3</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1500N; 2500N</td>
<td>0.1</td>
<td>0.2</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1500N; 1500N</td>
<td>0.2</td>
<td>0.2</td>
<td>70</td>
<td>263</td>
<td>384</td>
<td>9</td>
</tr>
<tr>
<td>1500N; 500N</td>
<td>0.1</td>
<td>0.1</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1000N; 2500N</td>
<td>0.6</td>
<td>0.3</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>500N; 2500N</td>
<td>0.5</td>
<td>0.2</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>500N; 1500N</td>
<td>0.4</td>
<td>0</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>500N; 500N</td>
<td>0.6</td>
<td>0</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>0N; 2500N</td>
<td>0.7</td>
<td>0.2</td>
<td>74</td>
<td>266</td>
<td>395</td>
<td>11</td>
</tr>
<tr>
<td>0N; 1500N</td>
<td>0.6</td>
<td>0</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>0N; 0N</td>
<td>0.8</td>
<td>0.1</td>
<td>70</td>
<td>267</td>
<td>384</td>
<td>8</td>
</tr>
</tbody>
</table>
measured on the specimens after welding - especially the start position of the specimen clamped with lower forces is affected (see Figure 5.8(a)). The tensile specimens taken from the start where the gap was 0.8mm had a noticeable more fragile behaviour than all other specimens. In terms of tensile properties it is therefore recommended to keep the clamping forces high enough as to guarantee only a very small gap (below 0.5mm approximately), especially at the weld start. A joint efficiency above 58% for yield strength and above 76% for rupture strength was obtained.

Three point bending tests showed that with an initial gap of 1mm between the plates to be joined, no complete 180° bend could be achieved. On the other hand, the bending angle was independent of the clamping force applied and a complete bend of 180° was obtained for the lowest and highest clamping forces.

Figures 5.9 show the microhardness evolution through the welded section on three lines through the thickness of the plates. One near the shoulder, one at mid-thickness and a third measurement near the weld root. Measurements were performed at three positions along the joint, but no variation could be found and therefore only one section is presented.

In terms of microhardness evolution, no significant difference could be detected between both plates. As can be verified, the alloy AA2198-T851 has a significant hardness loss below the shoulder. This happens, since the artificial ageing is destroyed due to the relatively high temperatures in this region and the naturally aged aluminium alloy AA2198 has a lower hardness. Heat treatment after the weld would lead to a more uniform hardness through the joint, but this is not always practical in real engineering problems and was therefore not made. It may also be seen that the width affected by lower material properties is higher in the case of the loosely clamped specimen,
which is expectable after looking at the macrographs shown below. Figures 5.10 show macrographs of the weld nugget region of two joints welded with different clamping forces only. Figure 5.10(a) shows a weld with 2500N initial clamping force in both directions, and Figure 5.10(b) shows a weld with 0N initial clamping force. It may be noticed that no big difference in the weld nugget may be detected based on the different clamping forces. Only the root area seems to be slightly wider when lower clamping forces are applied. The only limitation that was found in this work was the initial gap introduced on purpose in some of the specimens. In this case a small Lack of Penetration (LoP) could be detected in some cases, see Figure 5.11 for a macrograph of a specimen welded with an initial clamping force of 0N in vertical and horizontal direction and an initial gap of 0.25mm.

\[\text{(a) Approximate initial clamping force in horizontal direction: 2500N}\]

\[\text{(b) Approximate initial clamping force in horizontal direction: 0N}\]

**Figure 5.10:** Macrographs of the weld nugget zone depending on the applied initial clamping force.

**Figure 5.11:** LoP defect in the root area of a joint.
5.4 Influence of the clamping force on distortion

Since shape distortion is a difficult property to compare efficiently among all specimens, the parameter described in Section 2.4.2 was chosen, which may be easily compared and used for interpretation of the measured results. Input data for the calculated surfaces is the difference between the initial shape of the plate halves and the final shape of the welded specimen. This ensures that the initial distortion due to transport or manufacturing of the plate halves is filtered out of the result interpretation. Figure 5.12 shows the distortion of a plate clamped with 2500N in vertical and horizontal direction due to welding as an example. The initial distortion is subtracted from the final distortion in order to be able to see the influence of the clamping force on the distortion, without being significantly affected by the initial distortion of the plates.

Figure 5.12: Plate distortion due to FSW.

Figure 5.13 shows a comparison between the different overall distortion parameters depending on the clamping forces in horizontal and vertical directions. In this Figure it can be clearly seen that higher clamping forces in both the vertical and horizontal directions lead to reduced overall shape distortion. A small gap between the plates at the
beginning of the welding does reduce the higher distortions at low clamping forces and augments the smaller distortions with higher clamping forces.

![Figure 5.13](image)

**Figure 5.13:** Comparison of the overall distortion depending on the applied clamping forces.

### 5.5 Influence of the clamping force on residual stress

For residual stress measurements, the contour method was chosen due to its ability to provide through the thickness information on the residual stress distribution on a complete specimen cross section perpendicular to the section of interest. The contour method for residual stress measurement is used for the comparison of the residual stress state of two specimens, representative of the experiments. The first specimen chosen for this comparison was clamped with the maximum clamping force in both the vertical and horizontal directions, and the second specimen was clamped with the minimum clamping force in both directions.

The contour method was applied in four steps, for a detailed description see appendix B [237]. First a Wire Electro Discharge Machining (wEDM) cut was performed along the surface where perpendicular residual stresses were to be determined. Afterwards this surface was accurately measured, and after data conditioning, the shape was applied as displacement on a refined linear elastic FEM model for stress calculation. For this purpose 11700 points were measured on the surface of each of the plate halves using...
a Zeiss UPMC Ultra Coordinate Measuring Machine (CMM). Afterwards a bi-variate
spline surface was fitted to this point cloud using Matlab and its curve fitting toolbox.
The smooth spline surfaces were selected to have the minimum number of splines, while
still retaining a point representativity of over 99.8%. The measured shape was applied to
a FEM model with 149500 solid parabolic brick elements with 654039 nodes. A dedicated
workstation running Abaqus on Linux was able to solve the system of equations in about
one hour. The stresses perpendicular to the cut surface reported in Figure 5.14 were
afterwards extracted from the model.

![Residual stress distribution](attachment:residual_stress.png)

(a) FSW86 - horizontal and vertical clamping forces: 2500N; 2500N

![Residual stress distribution](attachment:residual_stress2.png)

(b) FSW102 - horizontal and vertical clamping forces: 0N; 0N

**Figure 5.14:** Residual stress distribution in welding direction at a cross section
through the centre of the plate. Residual stress is given in MPa.

As can be seen, both the shape distortion and the residual stress distribution are in-
fluenced by the applied clamping force. Higher clamping forces lead to lower distortion
and simultaneously to higher residual stresses through the thickness. The typical M-
shape residual stresses were found in both cases, but this is more obvious for the higher
clamping forces where a more uniform stress is obtained across the thickness.

Residual stresses are strongly concentrated beneath the shoulder region of the welded
plate. Outside of this region, zero and compressive stresses arise due to the need of
equilibrium of the tensile residual stresses in the welded region.

Since the contour method requires extrapolation of data to the borders of the plate due
to measurement restrictions, results near the borders may be exaggerated at some points
and should therefore not receive too much attention. Nevertheless good measurement results have been obtained.

This result could be expected, since in order to comply with energy equilibrium, it may be expected that stronger distortions lead to lower residual stresses as long as the input energy and other welding parameters were the same in both cases.

Synchrotron residual stress measurements were also performed on the same specimens for comparison purposes. Unfortunately neither the \( d_0 \) nor through the thickness information was acquired. Figure 5.15 shows the residual stress measured by the contour method compared to the synchrotron measurements. Synchrotron residual stress measurements were also performed on the same specimens for comparison purposes. Unfortunately neither the \( d_0 \) nor through the thickness information was acquired. Furthermore the quality of the obtained results was very low and no useful information could be extracted from the measurements. The noisy results are therefore only reported for qualitative assessment. The grain structure of the alloy AA2198 and its variation in the welded region make diffraction measurements very difficult. While these results are not useful, they reinforce one of the advantages of the contour method for residual stress measurement, which is only based on measurable deformations on a free surface and is not dependent on the grain structure.

Figure 5.15: Residual stress results, averaged across the specimen thickness as measured by both techniques. FSW86 - horizontal and vertical clamping forces: 2500N; 2500N; FSW102 - horizontal and vertical clamping forces: 0N; 0N.
5.6 Conclusions

As could be seen by the results presented before, a moderate but higher clamping force leads to lower distortions. This is similar to other manufacturing techniques, such as milling [238].

It is expected that higher distortion leads to lower residual stresses and lower distortion to higher residual stresses, since the energy input was similar in all specimens, generating the same amount of residual stresses. The need for an equilibrium between distortion and residual stresses leads to this interdependence between both effects. This is also the reason, why it is nearly impossible to eliminate both phenomena without adversely affecting mechanical properties through heat treatments.

Two main findings should be remembered. First, higher clamping forces lead to lower distortion as is also common empirical knowledge. Secondly, the higher clamping forces lead to a more uniform residual stress distribution through the thickness, but at the cost of an higher average residual stress in the nugget area.
Chapter 6

Distortion after High Speed Machining

In the aeronautical industry parts of the used panels may be machined from rolled plates until the required small thicknesses are reached. This often introduces distortions and redistributes the residual stresses caused by the rolling of the base material and possibly the machining process itself, leading to longer production times and higher manufacturing costs.

One method of reducing distortion in this machining process, is to machine the workpiece from both surfaces instead of only one, re-equilibrating the distortions. This leads however to the need of longer working times, since each workpiece has to be clamped at least twice. Cost reduction in this context would be greatly appreciated.

Rolling residual stresses and their influence on distortions are known for a long time. One method to relieve these stresses is by stretching. The present chapter deals with distortions that arise when machining rolled and already stretched plates. These distortions are generally of a small magnitude, but, since the intended application of the studied workpieces is the aeronautical industry, the very tight tolerances in this industry extend the need for further reducing distortion. A study of the origin of the distortions found was therefore made, different methods of measuring residual stresses were used and finally a model was built that can at least partially predict the measured distortions.
6.1 Distortion after machining complex geometries

The influence of stretching on mechanical properties and distortion after machining was already studied in 1959 by Bergstedt et al. [239]. A reduction of the distortion around 82% was found for stretching grades between 0.78 and 2.65% before ageing to the T651 condition. The longitudinal tensile properties were reduced by 5 to 10% by this procedure and greater losses occurred for the higher stretching grades, while the elongation seemed to be unaffected.

When releasing the vacuum clamping after machining of complex thin walled structures, such as shells or plates with pocketing, distortion is often found to a not acceptable degree. It is known that this distortion may depend among other parameters on the stretching grade of the workpieces.

Plates with a 1.0%, 1.5%, 2.0% and 2.4% stretching were machined at Horst-Witte-Gerätebau Barskamp KG (Bleckede, Germany) and the originated distortion was measured using the optical measurement system GOM mbH Pontos v6.1. Additionally, different machining sequences were used in order to find possible influences of the machining process itself on the final distortion and not only of the original residual stress state introduced by rolling and stretching.

Pockets with a depth of 3mm were machined into 6mm thick AA7075-T73 plates. The resulting distortions obtained for the differently stretched plates and by different milling sequences was observed.

6.1.1 Specimen and Experimental setup

For each stress relieve stretching grade, three 6mm thick AA7075-T73 plates were machined and their distortion was measured. For each stretching grade, Figure 6.1 shows the position of the three plates to be machined for result interpretation.
Charakterisierung des Grundwerkstoffes: Zugfestigkeit, Mikrohärte

1. VERSUCHSBLECH 2. VERSUCHSBLECH 3. VERSUCHSBLECH

IM-2.0-3.1 IM-2.0-3.2 IM-2.0-3.3 IM-2.0-3.4

2950 250 50 50

50 50 250 5

230 620 250 20

A B C D

Figure 6.1: Cutting plan of the specimens used.

Two pockets with the global dimensions $450 \times 250 \text{mm}^2$ and a depth of 3mm were machined in the plates. Reinforced areas about 50mm wide were left between the pockets. The inner corners of the pockets were round, since a 32mm diameter cutting head was used for machining.

Figure 6.2 shows the experimental setup used for the measurements using a camera based CMM.

Figure 6.2: Experimental setup for the optical distortion measurement.
In order to guarantee a uniform lighting, a 2000W halogen studio light with diffuser was used additionally to normal daylight.

The cameras were mounted on a 1.2m long camera holder at a distance of 2.6m to the plate to be measured. Schneider-Kreuznach lenses with a focal length of 50mm and a maximum aperture of f2.8 were used with an aperture of f8. This way the image was sharp in the whole measuring volume of about 750 × 750 × 750mm³. During calibration a pixel deviation of only 0.02 pixels was found (the limit defined by GOM mbH for good accuracy is of 0.04 pixels).

Distortion in the present case is defined as the variation of the geometry in relation to the initial state of the plate, which means that even if the initial state is not 100% flat, its distortion will be considered zero. This allows to directly identify the distortion introduced by the machining process itself.

Measurement deviation was determined by measuring each distortion state twice. The initial state was afterwards used to determine the measurement accuracy of each point by comparing both measurements for the same state. For all measured specimens, the average deviation of all points was below 0.02mm and the average standard deviation for all specimens was 0.02mm, being the worst deviation was 0.8mm. The measured results have enough accuracy to allow for a comparison between different specimens. The calculated surface, which represents the measured point cloud, is not able to represent each measured point exactly. Therefore the R²-value shows the safety with which all the points are represented as percentage. A high value, near 100%, means that the calculated surface adequately represents the measured point cloud. Using this procedure, noisy data can be reduced and results may therefore be compared between all the plates. All calculated surfaces represent the measured dots with a certainty of 97.5% in average and 1.8% of standard deviation.

### 6.1.2 Machining of pockets

Two distinct machining sequences were selected in order to be able to detect their influence on the measured distortions. In the first plate of each stretching grade (1.5%, 2.0% and 2.4%), IM-x.x-x.2, the pockets were machined in depth steps of 1mm, allowing distortion measurements during the machining process. Each of the steps applies less forging force which may have some effect on the final distortion of the plate. In plates IM-x.x-x.3 and IM-x.x-x.4, the pockets were machined at once as it normally happens in industry for the used thickness and depth. This leads to shorter production times and is therefore the preferred machining sequence. Results obtained for the first plate
may be compared to existing results for a stretching grade of 1% which were machined with the same sequence and parameters.

After each machining step, the distortion was measured. Therefore the plate was released from the vacuum clamping table and turned upside down, since the measuring points were located on the bottom side of the plate allowing the measurement of the whole plate and not only of the not machined reinforcement areas between the pockets. A corresponding coordinate system was applied to the measured results which shows the distortion of the plates as if they were lying on their bottom side during the measurement.

A Unisign type Uniport 4000 milling machine (year 2005) was used for this work and clamping was performed on a vacuum table. The machining parameters used were a rotational speed of 9350rpm and an advancing speed of 3150mm/min. In the case of the plate IM-1.5-2.2, the advancing speed was augmented to 4200mm/min in order to see if the distortion could be influenced to some degree by the advancing speed.

During the process, the rotation power and specimen temperature were assessed. The temperature was subjectively observed by touching the specimen immediately after the process, and the temperature in all cases was found to be low, near the temperature of the hand. Plates that were machined with the first sequence used 5 to 15% rotational power, while plates machined with the second sequence (3mm in one step) used 20 to 25% of the available rotational power. This leads to the conclusion that the second sequence introduces higher process forces than the first sequence, but for more precise results an adequate temperature and force measuring equipment should be used.

6.1.3 Results

Shape distortion is difficult to compare. Therefore the value chosen for comparison was determined by the method described in Section 2.4.2. This parameter is defined by determining the minimum distance between two planes parallel to the best fit plane of all measured points, through the minimum and maximum points respectively. It allows to consider only the shape distortion and effectively eliminates any apparent distortion due to a possible non-ideal measurement position of the specimen. Table 6.1 shows the global distortion value of all specimens. Below the Table, these results are shown graphically for all specimens.
Table 6.1: Overall distortion of all measured specimens.

<table>
<thead>
<tr>
<th>specimen</th>
<th>pocket depth [mm]</th>
<th>0mm</th>
<th>1mm</th>
<th>2mm</th>
<th>3mm</th>
</tr>
</thead>
<tbody>
<tr>
<td>IM-10-32</td>
<td></td>
<td>0.78</td>
<td>0.20</td>
<td>0.52</td>
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</tr>
<tr>
<td>IM-10-33</td>
<td></td>
<td>0.84</td>
<td>0.25</td>
<td>0.32</td>
<td></td>
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<tr>
<td>IM-10-34</td>
<td></td>
<td>1.02</td>
<td>0.69</td>
<td>1.21</td>
<td></td>
</tr>
<tr>
<td>IM-15-22</td>
<td></td>
<td>0.12</td>
<td>0.84</td>
<td>1.47</td>
<td>0.75</td>
</tr>
<tr>
<td>IM-15-23</td>
<td></td>
<td>0.11</td>
<td></td>
<td>0.71</td>
<td></td>
</tr>
<tr>
<td>IM-15-24</td>
<td></td>
<td>0.13</td>
<td></td>
<td>0.68</td>
<td></td>
</tr>
<tr>
<td>IM-20-32</td>
<td></td>
<td>0.09</td>
<td>0.84</td>
<td>1.54</td>
<td>0.73</td>
</tr>
<tr>
<td>IM-20-33</td>
<td></td>
<td>0.10</td>
<td></td>
<td>0.77</td>
<td></td>
</tr>
<tr>
<td>IM-20-34</td>
<td></td>
<td>0.08</td>
<td></td>
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<td></td>
</tr>
<tr>
<td>IM-24-42</td>
<td></td>
<td>0.13</td>
<td>0.99</td>
<td>1.76</td>
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</tr>
<tr>
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<td></td>
<td>0.07</td>
<td></td>
<td>0.74</td>
<td></td>
</tr>
</tbody>
</table>

While a slightly higher overall distortion is measured in the specimens stretched by 2.4%, the difference is small and no clear tendency can be detected using this parameter alone. A more detailed analysis of the influence of the specimen position on each plate and the stretching grade is presented later on. This analysis also considers the shape of the specimens, which can not be included into a single parameter. As can be seen, while the plates were not initially completely flat, the distortion is below or equal to 0.15mm in all cases.

6.1.3.1 Sequence 1

The distortion of the plate IM-1.5-2.2 was measured in 1mm steps up to the final pocket depth of 3mm, see Figures 6.3.
In order to enable a comparison between the different distortions which is not easily possible based on three dimensional surface plots, line plots of the centreline in X and Y directions are made. These lines are aligned to a zero angle and centred around zero. This way the distortion magnitude may be compared between different plates even if the overall shape is not exactly the same, leading to knowledge about the machining sequence and plate with the smallest overall distortion. Figure 6.4 shows the centreline in X and Y directions for easier comparison between the different plates.

![Figure 6.4: Distortion in the centrelines of the plate IM-1.5-2.2.](image)

As can be verified, in the rolling direction (X) the distortion reaches a maximum when the pocket has a depth of about 2mm, whereas in the direction perpendicular to the rolling and stretching directions the distortion stays the same independently of the pocket depth between 1 and 3mm.

The distortion of the plate IM-2.0-3.2 was measured in 1mm steps up to the final pocket depth of 3mm, see Figure 6.5.

![Figure 6.5: Distortion of the plate IM-2.0-3.2.](image)
Figure 6.6 shows the centreline in X and Y directions for easier comparison between the different plates.

![Figure 6.6: Distortion in the centrelines of the plate IM-2.0-3.2.](image)

The distortion of the plate IM-2.4-4.2 was measured in 1mm steps up to the final pocket depth of 3mm, see Figure 6.7.

![Figure 6.7: Distortion of the plate IM-2.4-4.2.](image)

Figure 6.8 shows the centreline in X and Y directions for easier comparison between the different plates.
In all three studied cases the distortion reached a maximum value after a pocket depth of 2mm and afterwards the distortion was reduced again. The reasons for this behaviour may be explained by the residual stress distribution. This information should be kept in mind when planning the machining sequence of rolled and stretched plates in order to avoid unnecessary distortions.

### 6.1.3.2 Sequence 2

Since in the aeronautical industry, (according to company Witte Gerätebau Barskamp KG), pockets of 3mm depth in 6mm thick plates may be machined without problems in one single step, further experiments are done with this sequence which is more interesting for the industry.

The distortion of the plate IM-1.5-2.3 was measured after the pocket depth of 3mm was reached, see Figure 6.9.
Figure 6.10 shows the centreline in X and Y directions for easier comparison between the different plates.

![Graph of distortion in X and Y directions](image)

**Figure 6.10:** Distortion in the centrelines of the plate IM-1.5-2.3.

The distortion of the plate IM-1.5-2.4 was measured in after the pocket depth of 3mm was reached, see Figure 6.11.

![Graph of distortion for pocket depth 3 mm](image)

**Figure 6.11:** Distortion of the plate IM-1.5-2.4.

Figure 6.12 shows the centreline in X and Y directions for easier comparison between the different plates.

![Graph of distortion in X and Y directions](image)
Chapter 6. *HSM*

**Figure 6.12:** Distortion in the centrelines of the plate IM-1.5-2.4.

The distortion of plate IM-2.0-3.3 was measured in after the pocket depth of 3mm was reached, see Figure 6.13.

**Figure 6.13:** Distortion of the plate IM-2.0-3.3.

Figure 6.14 shows the centreline in X and Y directions for easier comparison between the different plates.
The distortion of the plate IM-2.0-3.4 was measured in after the pocket depth of 3mm was reached, see Figure 6.15.

Figure 6.16 shows the centreline in X and Y directions for easier comparison between the different plates.
The distortion of the plate IM-2.4-4.3 was measured after the pocket depth of 3mm was reached, see Figure 6.17.

Figure 6.18 shows the centreline in X and Y directions for easier comparison between the different plates.
The distortion of the plate IM-2.4-4.4 was measured in after the pocket depth of 3 mm was reached, see Figure 6.19.

Figure 6.20 shows the centreline in X and Y directions for easier comparison between the different plates.

6.1.3.3 Influence of the different stretching grades

Plates IM-1.5-2.2, IM-2.0-3.2 and IM-2.4-4.2 were extracted from similar locations of the rolled and stretched plates and the machining sequence was also similar with comparable process temperatures and forces. The main difference between these three plates is the stretching grade which was 1.5\%, 2.0\% and 2.4\% respectively. Plates with a stretching grade of 1\% were previously measured and are included here for comparability [240]. Figures 6.21 show a comparison of the different distortions obtained in the centrelines of all three plates.
As can be seen, perpendicular to the rolling direction the difference is not very high, but in the case of the rolling direction, the highest stretching grade led to the highest distortion. As stated in the literature review, stretching reduces the rolling residual stresses, and introduces compressive surface residual stresses which normally are beneficial to the components’ life. In the present case, however, the surface was machined off, and therefore a new equilibrium had to be found. The originally flat plates with residual compressive stresses on the surface distort after clamping release due to the removal of the compressive surface layer. The highest stretching grade led to the highest distortion. It is therefore recommended to choose the stretching grade also based on whether the components will be machined or not.

Figures 6.22 and 6.23 show a comparison of the different distortions obtained in the centrelines of all three plates for similar locations on the original plate before cutting. In relation to the results shown before, the machining sequence was different here.
The same behaviour as for plates IM-x.x-x.2 is found for plates IM-x.x-x.3. A higher the stretching grade leads to a higher distortion after machining of the compressive superficial layer.

6.1.3.4 Influence of the position on the original plate

As was previously seen for plates with a stretching grade of 1% [240], the last machined plates have a different behaviour than could be expected. For the case of a 1.5% stretching grade, the distortion happens in the opposite direction as for all other cases, and the distortion for a stretching grade of 2.0% is found to be the strongest of all grades. This is most likely due to some unknown end conditions of the big rolled plate before cutting.
these specimens, which is common for rolled high strength aluminium plates. Additionally, not enough specimens were available for a safe statistical analysis. Therefore one reasonable conclusion which may be taken is that most likely the plates with a stretching grade of 1.5% show the lowest distortion values after machining of the pockets. Probably this happens due to the residual stresses introduced by the stretching procedure, which seem to be the lowest in this case.

Figures 6.24 to 6.26 show a comparison of the different distortions obtained in the centrelines of all three plates for equal stretching grades and different positions on the original plate.

**Figure 6.24:** Comparison of the distortion measured in the centrelines of the plates IM-1.5-2.x.

For the plates IM-1.5-2.x no significant difference between the distortion at different locations can be seen, except for the plate IM-1.5-2.4, where the distortion has the same magnitude, but the opposite direction.
For plates IM-2.0-3.x, only the last plate shows a significant larger distortion than the others, which remains to be explained but is in accordance with previous experiments where a different behaviour was also found for the last of the three plates [240].

For the highest stretching grade, the lowest distortion is found for the last plate, however the difference is small between all cases and due to the lack of further specimens no conclusion should be drawn based on these results other than that there doesn’t seem to be any significant difference between different positions on the plate, except for the last.

It was found that the different machining sequences tested did not change the final distortion. This means that the distortion seems to be more strongly related to the plate itself than to the machining process.
6.2 Origin of the distortion

Before any model predicting physical behaviour can be successfully built, the phenomena causing them should be at least partly understood. This increases the likelihood of success, reduces the number of necessary and expensive experiments and increases the confidence in the developed model.

When High Speed Machining rolled and stretched aluminium plates of the series AA7075 with temper T73, generally distortion occurs to a degree which is not anymore acceptable for the aeronautical industry. Since it is not known in advance how much a certain workpiece will distort, a significant amount of time and cost is lost when too much distortion is found after the workpiece is finished.

In order to determine the origin of these distortions, small plate samples with a stretching grade of 2.4% were machined on one of the surfaces and the resulting deformation was measured. One of the plates was machined in rolling direction while the second plate was machined orthogonally to the rolling direction.

This way it was expected to be possible to determine whether the resulting distortions originated from the initial stress state of the plate after rolling and stretching or if the distortion resulted from the machining process itself.

6.2.1 Specimen and experimental setup

Two 150×150mm² sized plates with a thickness of 6mm were cut from plate IMG-2.4-4.1 made of AA7075-T73. Figure 6.27 shows the location on the aluminium sheet from where the specimens were taken.

![Figure 6.27: Drawing of the extraction location of the machined and measured specimens.](specimen_location.png)
The small plates were machined in 0.3mm steps due to clamping restrictions. When lower thickness material layers are removed, less clamping force is required. It is necessary to guarantee that the small area of the plates clamped by vacuum only is sufficient for the process cutting forces. Distortion measurements were performed in the initial state (6mm thickness) and with thicknesses of 4, 3 and 2mm.

Figure 6.28 shows the experimental setup used for measuring the distortion by a optical coordinate measuring machine.

![Experimental setup for measuring distortion of the small samples.](image)

The cameras were mounted at a distance of 670mm to the specimen on a 0.5m long camera holder. The distance between the cameras was 350mm. Schneider-Kreuznach lenses with a focal length of 50mm and a maximal aperture of f2.8 were used with the aperture f16. This way the acquired images were sharp in the whole measuring volume of approximately 150x150x150mm$^3$. During calibration with the calibration object CP30 150x150 from GOM mbH, a pixel deviation of 0.03 pixels was achieved (a value below 0.04 pixels is recommended). The measurement area was solely illuminated by sunlight. The number of measured points on each specimen were approximately 80 on the plate machined perpendicularly to the rolling direction and 100 on the plate machined in rolling direction.

### 6.2.2 Machining

The material layers were removed by milling with a rotational speed of 7700rpm and an advancing speed of 3600rpm. For machining a 80mm diameter cutting head was used. Three parallel steps were needed to machine the whole surface of the plate. The machining direction was always the same (Y direction). On one plate the rolling direction
was parallel to the machining direction and on the second plate the rolling direction was perpendicular to the machining direction.

The temperature of the plate after each machining step was subjectively felt as slightly above the temperature of the hand. The power required by the machining head was near 20% of its capacity in the first step and below 5% in the consecutive steps. This can be explained by the fact that in the first step more material was removed than in the next steps (1.3mm in the first and 0.3mm in the next steps).

### 6.2.3 Measurement of the distortion

In order to be able to know the measurement accuracy, each step was measured twice, so that by comparing both measurements the error to be expected may be calculated. The initial state of the plate was used for calculation of this error. In all cases the maximum deviation between both measurements was 5\( \mu \text{m} \) and the average deviation was 1\( \mu \text{m} \), while the standard deviation of this error was 2\( \mu \text{m} \). The calculated surfaces represent at least 98% of the data points for all cases.

Figure 6.29 shows the distortion of plate IM-VRT-WR00 which was machined parallel to the rolling direction. It should be noted that only the vertical displacement of several points on the specimen surface was measured. The shown surface was calculated based on the measured points using a surface fit algorithm.
(a) Plate thickness 4mm  
(b) Plate thickness 3mm  
(c) Plate thickness 2mm

**Figure 6.29:** Distortion of the plate machined in rolling direction.

Figure 6.30 shows the distortion of plate IM-VRT-WR90 which was machined perpendicularly to the rolling direction. The machining direction was equal in all cases, the machining head moved in the Y direction.
Based on the comparison of Figures 6.29 and 6.30, it may be suspected that the machining direction does not have as much influence on the sample distortion as has the rolling direction. In order to be able to draw conclusions, the distortions on the centre-lines in the directions orthogonal and parallel to the rolling directions are plotted one against the other for both plates. Figures 6.31 to 6.33 show these results for all acquired thicknesses.

**Figure 6.30:** Distortion of the plate machined perpendicular to the rolling direction.
Figure 6.31: Distortion on the centreline for a thickness of 4mm in two different directions.

Figure 6.32: Distortion on the centreline for a thickness of 3mm in two different directions.

Figure 6.33: Distortion on the centreline for a thickness of 2mm in two different directions.
As can be verified in these Figures, no influence can be detected from the machining process itself, and the distortions are thought to be mostly related to the initial stress state of the rolled and stretched plate samples.

Rolling residual stresses arise during the rolling and stretching processes and are redistributed during HSM due to the removal of layers containing residual stresses which have to be re-equilibrated. It is therefore assumed that important residual stresses exist in these plates which have to be measured. The HSM process itself seems to be well enough optimised in the present case to not introduce additional residual stresses, at least not in the same order of magnitude as the already present rolling residual stresses. As will be discussed later on, HSM may influence the residual stress state very near the surface only. Residual stress has to be accurately measured in order to allow distortion prediction due to material removal.

### 6.3 Rolling residual stress measurement

After rolling, stress relieve by stretching may be applied to plates. The residual stresses created by the rolling and stretching processes lead to distortion as soon as the machining process is started. Since the influence of these residual stresses on the distortion of big thin walled structures produced by HSM is studied, the residual stresses have to be determined accurately. The very low magnitude of residual stress found in rolled and stretched plates creates a major problem to their accurate measurement, since most measurement techniques are not precise enough. In order to overcome this problem, several different measurement techniques were tested on comparable samples.

Several different measurement techniques were used in order to determine which yields the best results. Initially the non destructive Neutron, Synchrotron X-Ray Diffraction and XRD techniques were applied. Afterwards the semi invasive iHDT was used, and finally the fully destructive layer removal technique was applied to samples.

#### 6.3.1 Neutron diffraction

The residual stress state of different plates was determined using neutron diffraction. Measurements were performed before and after machining of pockets in AA7075-T73 plates in order to understand the influence of the residual stresses on the distortion of thin rolled and stretched aluminium plates. One of the fundamental advantages of most diffraction based residual stress measurement techniques is that comparison measurements may be performed at different times at the same location. The non
destructive nature of this kind of measurement allows the measurement of residual stress on the same sample that is to be machined, before and after the removal of material.

Three stretching values were considered in the measurements. 1% (already machined in prior experiments), 1.5%, 2.0% and 2.4%. In the first case, residual stresses were measured on a not machined plate and on an already machined plate. In the other cases, not machined plates were measured once, and after machining they were measured again. All specimens were made of AA7075-T73 and had the dimensions $650 \times 550 \text{mm}^2$ with 6mm thickness.

6.3.1.1 Specimen and measurement location

Three measurement locations according to Figure 6.34 were considered. In order to measure strain in all three directions, the plate had to be positioned in the beam three times. Since this procedure is time consuming, in order to achieve faster results, 5 plates were measured simultaneously at the same locations.

![Figure 6.34: Measurement locations defined for the experiment.](image)

The chosen locations were limited by the size of the measurable area of the used beam line, approximately $350 \times 350 \text{mm}^2$, and the number of points measured in this area were limited by the available beam-time.

Five points were measured through the thickness, being the measurement volume of rectangular shape with dimensions $30 \times 1 \times 1 \text{mm}^3$ as shown in Figure 6.35. All measured volumes were completely inside the specimen in order to avoid errors in the determination of the diffraction angle at the surface, when measurement volumes are only partially immersed in the body.

The residual stress measurement results for each point through the thickness represent an average value of the whole measurement volume, and no information may be obtained at the surface.
Figure 6.35: Details regarding each measurement point.

Figure 6.36 shows a picture of the specimens clamped in the measuring position of the ARES-2 beam line.

6.3.1.2 Obtained results

All measured residual stresses are in the range of -45 to 35 MPa, which are small values when compared to the expected error in Neutron diffraction measurements. The residual stress distribution shown in Figures 6.37 to 6.48 show similar tendencies. While the stresses in rolling direction (X direction) are mostly positive, the stresses in transversal direction (Y direction) are negative.
Figure 6.37: Measurement results on point 1 - stretching grade 1.0%.

Figure 6.38: Measurement results on point 1 - stretching grade 1.5%.

Figure 6.39: Measurement results on point 1 - stretching grade 2.0%.
Figure 6.40: Measurement results on point 1 - stretching grade 2.4%.

No significant differences between different plates can be determined by these measurements, since the measurement accuracy is probably too low. One possible conclusion that may be drawn comparing Figure 6.37(a) and 6.37(b), is that the machining process itself did not significantly affect the measured results. Higher quality measurements are however needed to verify this preliminary conclusion.

Figure 6.41: Measurement results on point 2 - stretching grade 1.0%.

Similar to what was found on another measurement point before, from Figure 6.41 it seems that the machining process did not influence the measured residual stresses significantly. The acquired magnitudes seem to be unaffected by the material removal process. The new equilibrium of the plate is now achieved through distortion. Since the plates were rigidly clamped during measurements, the distorted plate was actually
straightened to some degree, and therefore the measured stresses should be interpreted as a combination of residual and clamping induced stresses.

For the other stretching grades, only point 2 was measured again after machining for equipment availability reasons; the results are shown below.

**Figure 6.42:** Measurement results on point 2 - stretching grade 1.5%.

**Figure 6.43:** Measurement results on point 2 - stretching grade 2.0%.
As can be seen, the difference between the measured residual stresses before and after machining of pockets is within the expected measurement error. It should be mentioned that the specimens were rigidly clamped during measurement, which means that their distortion was reduced and therefore the measured stresses before and after machining are comparable and not influenced by bending induced stresses at the same locations.
Figure 6.46: Measurement results on point 3 - stretching grade 1.5%.

Figure 6.47: Measurement results on point 3 - stretching grade 2.0%.

Figure 6.48: Measurement results on point 3 - stretching grade 2.4%.
From the available results it may be concluded that Neutron diffraction based residual stress measurement has limited applicability to the present case due to its relatively high error level and due to its inability to measure surface residual stresses. A higher though-the-thickness resolution is also required for accurate distortion predictions. As an important result of the performed measurements, it may be concluded that the machining process itself does not apparently change the residual stress field in the remaining material.

### 6.3.2 Synchrotron XRD

In order to achieve a higher resolution and lower noise, but still using a non destructive technique, Synchrotron XRD measurements were performed on one specimen. Due to the extremely high noise in relation to the values to be measured, the results are not reported. Among others, problems arose due to missing $d_0$-spacing measurements, insufficient long beam time and difficulties in the raw data interpretation.

### 6.3.3 Incremental Hole drilling technique

Since the non-destructive techniques tested have not provided the necessary information for accurate distortion prediction, a further step was to advance into the semi-destructive techniques for residual stress measurement. The iHDT was chosen for this task, due to its availability, and the fact that only a small hole has to be drilled in the workpiece. In most cases the whole plate could still be used for production after residual stress measurement, since the holes may be drilled in locations where material will later be removed.

Residual stress was measured on specimen IM-1.5-2.1 stretched by 1.5% at 9 different locations in the centre of the plate using the iHDT. Since the stresses are assumed to be approximately constant in plane direction, this allows to verify the measurement accuracy of the iHDT. Stresses in rolling and transversal direction were recorded. It may be expected that all points lead to almost the same values, since measurements were performed in a squared area of $300 \times 300 \text{mm}^2$ in the centre of a rolled plate with a size of $650 \times 552 \text{mm}^2$, where variations are only expected near the borders. These measurements were performed by the Instituto de Engenharia Mecânica e Gestão Industrial (INEGI), and more details may be found in the respective technical report [241]. Figures 6.49 show the residual stress in rolling and transversal directions measured on 9 points and their corresponding average value. Figure 6.50 shows the evolution of the standard deviation as a function of the measurement depth.
The variation of residual stresses across the plate is in the same order of magnitude as the measurement precision. The first measurements near the surface should not receive too much attention with the hole drilling method, since these are easily affected by experimental errors. Unfortunately the iHDT did not lead to enough through the thickness information for a complete model of the plate, since only the upper 1mm could be partially measured. Anyway, more information could be gained using the iHDT if material is removed in order to measure at greater depths. One information that may be retained from the above results, is the fact that the iHDT measurements seem to deliver reliable results with a standard deviation below 3MPa in a depth near 0.2 to 0.8mm. Outside of this range, the difference between measured results raises, and results should therefore not be trusted if high accuracy is required.
When the surface of a rolled and stretched plate is machined, distortion may appear due to the partial removal of residual stress, and the remaining residual stresses will be redistributed in order to further guarantee equilibrium. As determined before, the material removal process itself did not seem to generate significant residual stresses in the studied case. In order to verify this observation, AA7075-T73 plate specimens stretched by 2.4% were measured in three production stages. The first plate was rolled and not machined, the second plate was machined on one of the surfaces, and the third one was machined on both surfaces. The different residual stress states and distortions of these three plates were compared, using the iHDT and a CMM.

### 6.3.3.1 Milling of the plate

The analysed plates were cut from the plate IM-2.4-4.1. Figure 6.51 shows the cutting plan for the specimens.

![Cutting Plan](image)

Figure 6.51: Cutting plan for the tested specimens.
6.3.3.2 Distortion measurement

The bottom surfaces were measured by a touch probe in order to compare the distortions in the three different stages of the machining process. A Zeiss UMPC Ultra coordinate measuring machine from Centro de Apoio Tecnológico à Indústria Metalomécanica (CATIM) based on touch probing by a 4mm diameter Ruby tip was used for surface deformation measurements, see Figure 6.52. For obtaining comparable results, approximately the same set of points was measured on all plates. The positioning of the plates is guaranteed by mechanical stops. Distortion was measured on the three plates also measured by the iHDT.

![Specimen with one machined side during the CMM distortion measurement.](image)

**Figure 6.52:** Specimen with one machined side during the CMM distortion measurement.

6.3.3.3 Residual stress measurements

Residual stress was measured on the top and bottom surfaces of the specimens on one point in the centre. Four specimens were measured at Faculdade de Engenharia da Universidade do Porto (FEUP) using the iHDT. Further similar specimens were stored for measurements with non-destructive techniques for comparison.

Vishay CEA-13-062UL-120 strain gauges and the Vishay RS200 milling guide were used. Dentist inverted cone drills with a diameter of 1.6mm from Brasseler United States of America (USA) where chosen, since these are recommended by Vishay.
6.3.3.4 Results

Both, residual stresses and distortion, were measured on the same specimens. For the not machined plate (IM-2.4-ES-0.2) residual stress measurements were performed using the iHDT, see Figure 6.53.

![Graph](image1)

**Figure 6.53:** Surface residual stresses measured by the iHDT on plate IM-2.4-ES-0.2.

Since a relatively high noise was found during these measurements, a similar specimen was measured for comparison. For the second not machined plate (IM-2.4-ES-0.3) residual stress measurements were also performed using the iHDT, see Figure 6.54.

![Graph](image2)

**Figure 6.54:** Surface residual stresses measured by the iHDT on plate IM-2.4-ES-0.3.

Figure 6.55 shows the results for the plate machined only on one side (IM-2.4-ES-1.2).
Finally, in Figure 6.56 are shown the results for the plate machined on both sides (IM-2.4-ES-2.2).

As can be seen, compressive residual stresses in the order of -5 to -10MPa were found in the upper layers of the material, while compressive residual stresses seem to exist in thickness direction, which is also necessary to guarantee the equilibrium in the plate. Anyway, the very low magnitude of these results leads to problems with the accuracy of the iHDT, therefore the results should be taken as estimations only.

Distortion of the plates was measured afterwards. For this specimens the plate before machining was considered as being distortion free, since the plates seemed to be flat before machining and no precise enough equipment was available when performing the milling operations for confirming these observations. Figure 6.57 shows the measured distortions of both machined specimens.
In the case of the plate which was machined only on one side, the observed distortion is strong, since one superficial layer is removed. The residual stresses in a rolled and stretched plate are assumed to be symmetric in relation to the mid-plane of the plate and therefore a new equilibrium state has to be found by the plate when stresses are partially removed from one side only. The distortion is greatly reduced by machining the plate from both sides, since the residual stress distribution is expected to be symmetric in relation to the central plane of the plate and therefore only symmetric parts of the residual stress are removed. A new stress equilibrium is reached without distortion, creating the necessary force and moment equilibrium without bending. Unfortunately the quality of the residual stress measurement results by the iHDT was not high enough to fully explain this behaviour, therefore a more accurate technique, although destructive was chosen.

### 6.3.4 Layer removal technique

The layer removal method for residual stress measurement is a fully destructive method based on the measurement of the distortion of a specimen due to the removal of layers of material with residual stresses. Schajer recommends the use of the layer removal technique when stresses are known to vary through the thickness, but are uniform parallel to the surface [119]. This means that this method may be used if for a certain depth through the thickness, the stresses are known to be approximately uniform across the plate.
Hospers et al. shows the layer removal technique applied to rolled sheet metal [126], which is similar to the intended application. This author derived equation 6.1 for determining the residual stress incrementally, which will be used in the present work.

\[
\sigma_{xi} = \frac{E}{12(1-\nu^2)} \left( t_i^3 k_i - t_{i-1}^3 k_{i-1} \right) - \frac{1}{2} \Delta e_i \sum_{n=1}^{i-1} \sigma_{xn} \Delta e_n
\]

As described by Hospers et al. [126], a layer of thickness \( \Delta e_i \) is removed in step \( i \). Afterwards, both the thickness \( t_i \) and the curvature \( k_i = \frac{1}{R_i} \) is measured, see Figure 6.58. The values with index \( i - 1 \) are already known from the preceding step. More information may be found in Section 2.3.1.6.

This residual stress measurement technique was performed on a Computer Numerical Controlled (CNC) milling machining with measurement capabilities. One of the specimens’ surfaces was machined stepwise and measurements of the distorted shape were performed after each step.

The layer removal technique was developed with material removal techniques similar to electropolishing in mind. In the present case HSM is used due to its availability and proven little influence on plate distortion, see Section 6.2.

Robinson et al. chose HSM with a spindle speed of 8600rpm and an advancing speed of 3600mm in order to minimise the introduction of residual stresses by the machining process of the AA7449 aluminium alloy [242]. During material removal, the distortion of the material block was measured and found to increase with increasing material removed.

Denkena et al. studied the influence of different parameters on the machining introduced residual stresses. While they found a significant influence of the machining parameters on the surface and sub-surface residual stresses, no information is given regarding deeper measurements [243]. The maximum advancing speed investigated was 1500mm/min which is less than half of the speed used in the present work. The work by Denkana et al. suggests that further studies should be performed.
Since in the present work the aim is to determine the distortion in high speed machined aluminium sheets, the same layer removal process is deemed appropriate for the residual stress measurements. It is assumed, that even if the milling process introduces some small residual stresses, these will also be introduced in the high speed machined plates, and therefore even with some possible magnitude difference of the determined residual stresses the distortion may still be predicted.

The specimen used for this experiment was a 200mm long, 20mm wide and 6mm thick AA7075-T73 plate with a stretching grade of 2.4%. Figures 6.59 show a scheme and a photograph of the specimens.

![Figure 6.59: Specimen used in the experiment.](image)

This experiment was divided into three parts. Removal of several layers of material by machining, measurement of the distortion after each machining step and data analysis. Each step defined by machining a layer, measuring the resulting distortion and the specimens’ thickness until the next step is prepared took between 3 to 5 minutes. This means that only a few hours of work are needed for performing these residual stress measurements.

Machining was performed on a Fadal VMC 3016L vertical milling machine using a 50mm diameter cutting bit at 5000rpm rotational speed and an advancing speed of 1200mm/min. These parameters guarantee a very reduced introduction of additional residual stresses during the machining process. Figures 6.60 show the machining process and Figures 6.61 show the clamping arrangement with two parallel clamps.

Measurement of the distortion was performed on the same milling machine, using a measuring head capable of a precision in the range of a few microns. It was therefore deemed appropriate for the required accuracy below 0.01mm. Figure 6.62 shows the device used for measurement.
(a) machining of a layer of material  
(b) vertical cutter used for machining

**Figure 6.60:** Machining of the layers.

(a) clamping arrangement  
(b) detail of the clamping arrangement

**Figure 6.61:** Clamping arrangement during machining.

(a) measurement being performed  
(b) displayed value

**Figure 6.62:** Device used for distortion measurement.
Figure 6.63 shows the points measured after the fifteenth layer was removed and the superimposed approximated circle as an example.

Figure 6.63: Illustration of the approximation procedure of the measured distortion points by part of a circle.

Figure 6.64 shows all measured distortions on the specimens in the rolling and transversal directions superimposed on the calculated arcs with the obtained radius of curvature for each layer removal step. Calculation of the radius of curvature was aided by the circle fit algorithm developed by Bucher [244].

Figure 6.64: Measured distortion on two specimens after each of the layer removal steps (measured and smoothed data and the calculated arcs based on the determined curvature).

Figures 6.65 show the evolution of the curvature of both specimens, as measured, and smoothed.
The thickness was measured on five points along the specimen, in order to assure that the layer was removed with a constant thickness. These measurements showed that the machining process was precise enough for the current case, but some differences were detected between measurements which may influence the calculated results. For future works it is therefore suggested to optimise the clamping procedure. Figure 6.66 shows a picture of the setup used for thickness measurement. As can be seen from Figure 6.67, the thickness of the specimen was approximately constant along the specimen up to a thickness of nearly 4mm, but afterwards, the machining process did not guarantee a constant material thickness removal. This may be related to the noise in the measured curvature shown in Figure 6.65 starting at approximately the same thickness.

**Figure 6.65:** Curvature of the specimens as measured (dots) and smoothed (thick curve).

**Figure 6.66:** Measurement of the specimen remaining thickness after machining at 5 points along the specimen.
(a) rolling

(b) transversal

**Figure 6.67:** Difference in thickness at each of the 5 measured points in relation to the average thickness as a function of the average thickness of the specimen after each machining step.

The analysis of the results is based on the radius of curvature and thickness of the specimens after each step of the measuring process. Some filtering is involved in order to reduce the noise from the experimental data. The first filter is applied to the curvature, where a smooth evolution of the curvature is expected as a function of the remaining thickness of the specimen. The second filter is applied to the final residual stress measurement results shown in Figures 6.68. This Figure shows the measured residual stresses on both specimens. The stair like curves represent the actual measured values extended to the layer thickness, and the other curve represents the averaged data of the measured values.

**Figure 6.68:** Measured residual stresses on both specimens.

Filtering of the measured data was needed and therefore the obtained precision may have been reduced. Considering the low residual stresses, normally prone to high errors
in other experimental techniques such as the hole drilling technique or diffraction based methods, the present method is still deemed appropriate.

In order to define the complete residual stress distribution through the specimen thickness, some points have to be considered:

- The distribution through the thickness is considered to be symmetric in relation to the mid-thickness in order to guarantee the equilibrium of moments across the thickness.
- Equilibrium between tensile and compressive residual stresses exists in the plate and has therefore to be guaranteed and if necessary enforced.
- The actual measured results should be checked additionally to the smoothed data in order to better evaluate peaks.

Figure 6.69 shows the residual stress distribution in both directions as measured and the residual stress distribution applied to the finite element model later on. The equilibrium is guaranteed by subtracting the average stress along the thickness from the measured distribution, since it is known that this average stress has to be zero in order to guarantee the force equilibrium. This correction is necessary due to the difficulty to perform perfect measurements, given the very low magnitudes of the data being measured. Even with a different stretching grade, the results shown in section 6.3.3 are in reasonable agreement with the results presented in this section. A comparison may be made for a depth position between 0.2 and 0.8mm, and the order of magnitude of the measured stresses is similar.

Figure 6.69: Residual stress in both plate directions.
In order to verify the measured residual stresses, these are applied to a FEM model of the specimen where the stresses were measured, and the calculated curvature after simulating layer removal is compared to the measured curvature. Figure 6.70 shows the curvature variation with specimen remaining thickness of both, the measured specimen and the calculated specimen. The simulated curvature was obtained by introducing the measured residual stresses into a model with the geometry of the specimen.

![Figure 6.70](image)

**Figure 6.70:** Curvature as a function of the remaining thickness of the specimen used for residual stress measurement. Measured data before and after filtering and calculated curvature.

As can be seen, the curvature variation is calculated with a high degree of accuracy, leading to the conclusion that the measured residual stresses are near the real values. The offset of both curves is due to the initial distortion of the real specimen which was not modelled. A further guarantee for the high quality of the measured results may be obtained by calculating the residual stress distribution based on the calculated distortion of the specimen due to the measured residual stresses. The measured and calculated residual stress distribution is very similar as can be seen in Figure 6.71.

Residual stress was measured independently in both directions on the specimen and joined by the elastic superposition principle. The accuracy of the measurements seems to be high enough for modelling purposes.
6.4 Prediction of the distorted shape

The FEM together with the measured residual stress distribution though the plate thickness is used to predict the distortion due to machining of pockets in rolled plates. The predicted results are compared to measured results in order to validate the model.

The finite element model for the pocketing process is a simple linear elastic model, which completely ignores the production process. The final geometry of the workpiece is modelled, and the residual stresses present in the plate, originating from the rolling and stretching processes, are introduced as initial conditions.

6.4.1 Mesh

Figure 6.72 shows the mesh used in the FEM model, defined by 663641 nodes and 148824 quadratic elements (Abaqus “C3D20R”). Reduced integration was used. Twelve elements are used through the thickness of the plate in order to be able to generate a satisfactory resolution of the applied rolling residual stresses.

6.4.2 Boundary conditions

As boundary conditions it was chosen only to fix three nodes in thickness direction Z, and one node additionally in X and Y directions in order to eliminate the rigid body motion. The constrained nodes do have negligible reaction forces and therefore do not influence the obtained distortion.
6.4.3 Initial conditions

The residual stress distribution obtained by the layer removal technique is applied to the model. Residual stresses are applied to the whole elements as initial conditions. This means that only a discreet distribution may be represented by the model. The applied residual stresses can be seen in Table 6.2.

Table 6.2: Residual stresses applied to the element layers as initial condition.

<table>
<thead>
<tr>
<th>layer name</th>
<th>$\sigma_{RSx}$</th>
<th>$\sigma_{RSy}$</th>
</tr>
</thead>
<tbody>
<tr>
<td>layer_00_05</td>
<td>5.96</td>
<td>-6.64</td>
</tr>
<tr>
<td>layer_05_10</td>
<td>6.16</td>
<td>-5.55</td>
</tr>
<tr>
<td>layer_10_15</td>
<td>3.62</td>
<td>-1.74</td>
</tr>
<tr>
<td>layer_15_20</td>
<td>-0.9</td>
<td>0.54</td>
</tr>
<tr>
<td>layer_20_25</td>
<td>-5.83</td>
<td>3.41</td>
</tr>
<tr>
<td>layer_25_30</td>
<td>-9.02</td>
<td>9.97</td>
</tr>
<tr>
<td>layer_30_35</td>
<td>-9.02</td>
<td>9.97</td>
</tr>
<tr>
<td>layer_35_40</td>
<td>-5.83</td>
<td>3.41</td>
</tr>
<tr>
<td>layer_40_45</td>
<td>-0.9</td>
<td>0.54</td>
</tr>
<tr>
<td>layer_45_50</td>
<td>3.62</td>
<td>-1.74</td>
</tr>
<tr>
<td>layer_50_55</td>
<td>6.16</td>
<td>-5.55</td>
</tr>
<tr>
<td>layer_55_60</td>
<td>5.96</td>
<td>-6.64</td>
</tr>
</tbody>
</table>
6.4.4 Results

The distortion obtained by the FEM model is shown in Figure 6.73(b) compared to the measured distortion in Figure 6.73(a). Distortion measurement was performed using the optical distortion measurement system GOM mbH Pontos, and a smooth surface was interpolated through these points using The Mathworks Matlab software [245].

As can be seen, the distortion predicted by the FEM model based on the measured residual stresses is identical in shape, but different in magnitude.

Two perpendicular centrelines were chosen through the plate in order to be able to better compare both results. The measured results were aligned with the horizontal for comparison, and both results were aligned to zero. This allows to compare the shape distortion only, see Figure 6.74.

Taking into account the precision of the GOM mbH Pontos measurement device and the variation between specimens, the difference between the measured and calculated distortion results in the transversal direction is negligible. In the longitudinal direction, the model predicts a significantly higher overall distortion of the specimen. This may be due to differences in the residual stress state of the different plates, problems with the actual measurement or problems with the prediction model. Further experiments are recommended before using this model in a production environment.

The simulated distortion of the complex geometry is identical to measured results in shape, but different in magnitude. The main problems in this case arise due to the
combination of the very low residual stress level found in the plates with the high precision required for the prediction of the plate distortion. The required residual stress measurement precision is even higher than the precision available for measurement.

For verification purposes the same distortion prediction approach is applied to a 150mm ×150mm² big plate. It is expected that the calculated distortion is similar to the measured distortion shown in Figure 6.57(a). Results for this simulation, are shown in Figures 6.75 and 6.76.
Figure 6.75: Distortion measured by CMM on plates IM-2.4-ES-1.2 and its simulation (5 mm remaining thickness).

In this case the distortion measurements used for comparison were made on a touch-probe-based CMM which normally leads to higher quality results. In this case the simulation leads to better results in rolling direction than in transversal direction. Another comparison is therefore made to Figure 6.29 of a similar specimen, see Figures 6.77 to 6.79.
Chapter 6. *HSM*

Figure 6.77: Distortion of a machined plate and its simulation (4mm remaining thickness).

(a) Measured distortion

(b) Simulated distortion

Figure 6.78: Centrelines representing the distortion measured by GOM mbH Pontos and its simulation (t=4mm).

(a) Rolling direction

(b) Transversal direction

Figure 6.79: Distortion measured by GOM mbH Pontos and its simulation (t=3mm).
As can be seen, in the last example, the distortion in rolling direction is overestimated. One aspect that may also explain some differences between the models and the real plates, is the fact that initially flat and exactly 6mm thick plates were modelled, while some small initial distortion may exist on real plates with slight thickness differences.

6.5 Conclusions

While there are aspects of the present work that should still be optimised, such as the clamping procedure during the layer removal process, the main tasks of this work were accomplished, and a model was demonstrated which can predict part distortions at least partially. Furthermore, the developed measurement technique and model can be executed with the equipment available at Witte Gerätebau Barskamp KG, and does therefore not introduce great logistical problems if an introduction into manufacturing is planned at a later stage. While most residual stress measurement techniques do not provide all information necessary for accurate distortion prediction, the layer removal technique provides enough information for distortion prediction by the FEM. It was shown that it is, up to a certain extent, possible to predict the distortion of complex thin walled structures by measuring the residual stresses before manufacturing, and applying these to a model with the final shape of the desired structure. Figure 6.80 shows a possible scenario of how such a model could be integrated in the normal workflow of the company.

While traditionally the aluminium sheets are sent directly to the machining department, in the new workflow, small specimens from the same sheet could be used for residual stress measurements, which could help to predict the final part distortion, leading to shorter production times, and less time spent with sheets that will distort too much for the pretended application.
(a) Normal workflow

(b) Enhanced workflow

Figure 6.80: Enhancement of the workflow with the developed model.
Chapter 7

Fatigue of Friction Stir Welded joints under biaxial loading

While integral metallic structures are state of the art for several engineering applications, in the aeronautical industry this design concept is still not widely used. Instead, the traditional approach of a differential structure connected by mechanical fasteners is very common. Integral metallic structures have received increased interest from the aeronautical industry, since cost and weight reduction may be achieved simultaneously using such a design concept. The capability to predict the fatigue crack growth behaviour in integral airframe structures is crucial for their application. Aircraft fuselage skin panels are thin and subjected to a biaxial stress state, the consequences of which may not always be sufficiently well characterised using uniaxial experiments. Biaxial fatigue testing is therefore important. The aircraft fuselage shell should be considered as a pressure vessel subjected to additional external loads. Therefore significant in-plane loads are present in the longitudinal and circumferential directions. Biaxial fatigue testing is however a complex effort, since special equipment is needed, and the experimental setup is complex and expensive. The preparation and setup of biaxial fatigue crack growth tests is described aiming at simulating realistic service loading conditions in the aircraft fuselage skin. Initially the development of the test specimen is described, which is followed by the experimental setup for testing and Fatigue Crack Growth measurements. Finally, biaxial fatigue experiments are described and their results analysed for the longitudinal and transversal weld directions.

It is shown that the rolling direction of the selected aluminium alloy strongly affects the crack growth path. Specimens welded orthogonally to the rolling direction, with the crack growing parallel to the weld, exhibit a lower fatigue life than specimens welded parallel to the rolling direction, with the crack growing parallel to the weld.
7.1 Specimen design

Traditional aircraft structures are mostly manufactured by riveting. This differential joining approach leads to assembled structures, which can inhibit the propagation of skin cracks by intact stringers or frames. Cracks may not easily grow from one panel into the next. Integral structures however, which may be fabricated for example by high speed machining or various welding processes, do provide a continuous crack path. Therefore their fatigue crack growth behaviour has to be thoroughly studied before this type of innovative design concept may be used in the aeronautical sector. While such a design approach requires a significant investment in damage tolerance studies before application, important savings may be achieved in the long run. Reduced part count and lower inspection requirements due to the lack of overlap joints are factors favouring the introduction of more integral parts in aeronautical structures.

7.1.1 Basic geometry for static tensile load

The design of the basic cruciform specimen geometry was based on the review of existing designs [179, 181, 191, 246]. In an early specimen design, slots in the loading arms were successfully used at HZG in previous projects. An example specimen for static tensile load tests is shown in Figure 7.1. The radius of the fillet between two adjacent loading arms is 3mm in this case.

Figure 7.1: Photo of a HZG test sample for static tensile loading test with load-diffusion slots [246].
7.1.2 Revised geometry for cyclic load

For the purpose of this study, testing specimens under cyclic loads, the basic geometry shown in Figure 7.1 was revised based on the finite element analysis. To prevent any unwanted cracks from initiating at the round fillet between two adjacent loading arms, the radius must be large enough to reduce the stress concentration at these four hot spots. While this approach could lead to cross-loading between the arms, this didn’t seem to be an issue based on strain gauge results from dummy specimens tested before the welded specimens. Slots were not used in these experiments due to prior experience with this kind of specimens in the laboratory.

A finite element model was generated for this purpose using very fine mesh around the fillet curvature in order to model the stress concentration accurately. Since cyclic loads could also cause fatigue failure at the small radius of the slots, test specimens planned for fatigue testing do not contain these slots. Selected fillet radii (r) were modelled. Calculated stress contour maps are shown in Figures 7.2 for r = 3 and 12mm. It shows that the Kt is significantly reduced with the higher radius. To minimise the risk of crack initiation from the corner fillet, r = 20mm was selected for the final specimen design, where the Kt calculated by the finite element method is 3.2 [203].

![Stress Contours](image.png)

**Figure 7.2:** Von Mises stresses calculated by the finite element method for fillet radius (a) 3mm and (b) 12mm at applied biaxial stress of 100MPa:100MPa. Stress concentration factor $K_t = \frac{\text{maximum von Mises stress}}{\text{applied uniaxial stress in y-axis}}$.

Using the S-N curve of the AA2198-T8 aluminium alloy, the lower and upper bounds of fatigue crack initiation life were estimated at $1 \times 10^6$ to $6 \times 10^6$ cycles. This estimated fatigue life at a point located in the fillet is sufficiently longer than the cycle numbers required for fatigue crack propagation from a pre-crack of 40mm length in the specimen.
centre. Fatigue crack growth life was evaluated to be in the order of $10^5$ [203]. Since the aim of the research reported in this and follow-on papers is about crack propagation, radius $r = 20\text{mm}$ was chosen for the testing specimens.

### 7.2 Testing procedure

#### 7.2.1 Test rig and setup for biaxial loading

The triaxial testing machine used for this work has six independent cylinders. Four of these cylinders are used for the biaxial fatigue experiments. Each arm of the specimen is clamped to one cylinder and the corresponding load cell arrangement. The modal control of the machine assures that the centre of the specimen always stays in the centre of the machine.

The triaxial servo-hydraulic testing machine was developed by Schenck, later acquired by Instron Structural Testing Systems. All six cylinders may be controlled simultaneously and independently. This triaxial testing machine is equipped with six 1MN servo controlled hydraulic cylinders. Only four of them were used in modal control in the present work. The centre of the specimen always stayed in the centre of the machine due to this control scheme. Figure 7.4 shows photographs of the setup that was used in this experiment. As can be seen from Figure 7.4(a), the specimen was hanging unloaded inside the rig when the zero level was set for strain measurement. The clamping system attached to the cylinders was aligned before inserting the specimens, and special care was taken during the specimen setup to guarantee that also the specimen was correctly aligned.

![Figure 7.3: Loading rig used for biaxial testing.](image)
Chapter 7. Fatigue of Friction Stir Welded joints under biaxial loading

Figure 7.4: Photos of the experimental setup.

The servo-hydraulic controller values can be seen in Table 7.1. The servo-hydraulic controller uses a delay (L) in addition to the standard proportional (P), integral (I) and derivative (D) parts of the control algorithm in order to guarantee the required relation between all cylinders. Using these values, the required phase shift of 0° was achieved at a loading frequency of 4Hz.

Table 7.1: PIDL values for the Instron servo-hydraulic modal controllers.

<table>
<thead>
<tr>
<th></th>
<th>4Hz</th>
<th>P [dB]</th>
<th>I [/s]</th>
<th>D [ms]</th>
<th>L [ms]</th>
</tr>
</thead>
<tbody>
<tr>
<td>X-sum</td>
<td>4</td>
<td>0.6</td>
<td>0</td>
<td>75</td>
<td></td>
</tr>
<tr>
<td>Y-sum</td>
<td>4</td>
<td>0.6</td>
<td>0</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>X-difference</td>
<td>5</td>
<td>0.2</td>
<td>0</td>
<td>0</td>
<td></td>
</tr>
<tr>
<td>Y-difference</td>
<td>5</td>
<td>0.2</td>
<td>0</td>
<td>0</td>
<td></td>
</tr>
</tbody>
</table>

7.2.2 Load measurement uncertainty

The load cells may be assumed to measure correct values, since the machine was inspected and updated by the manufacturer for these experiments. Nevertheless, before the experiment was started, the maximum uncertainty from the unloaded load cells was determined in order to gain insight into the best possible force control values. The maximum uncertainty from both axes obtained for all specimens with the hydraulic system turned off was determined to be 0.08 to 0.22kN on the X-axis and 0.05 to 0.26kN on the Y-axis for all specimens. No higher accuracy should therefore be expected from this setup. It should be kept in mind that the load cells have a capacity of 1000kN, which means that 0.1kN is equivalent to a voltage drop of only 1μV. This uncertainty level was verified during the experiments and did apparently not seem to influence the
experiments. The uncertainty is influenced by various factors and cannot be controlled completely in such a complex setup.

Strain measurements were performed during the complete clamping process in order to guarantee that all information regarding each of the specimens is conveniently stored. This data may be used in order to understand differences in the numerical models, which do not simulate the initially distorted shape of the specimens due to the welding process.

7.2.3 Crack measurement device

Crack measurement may be performed using an optical microscope for highest accuracy, see Figure 7.5. In the present setup, a Charge Coupled Device (CCD) camera is connected to a Zeiss optical microscope with a Zeiss fiber optics light source. This permits a good identification of the crack tip location. In order to define the coordinates of the crack tip in the global coordinate system of the specimen, digital scales are attached to a table in the X and Y directions, see Figure 7.5(a). These scales and the microscope allow to achieve accuracies better than half a millimetre, which was adequate for the current measurements, where the crack tip position was recorded with an accuracy of one tenth of a millimetre. In Figure 7.5(b) the initial Electro Discharge Machining (EDM) notch of a specimen is shown as an example. The end of the notch was additionally cut with a very thin razor blade for easier fatigue crack initiation.

Using this setup it was possible to detect the crack tip on all specimens. Only the ergonomics of this setup need improvement, since for best illumination the light source had to be handled manually inside the testing rig. It was decided not to apply automated crack tip detection algorithms, since in the author’s own experience these techniques are

![Figure 7.5: Crack tip detection and crack length measurement.](image-url)
not yet reliable enough to replace manual crack tip detection in complex setups [247]. Poncelet et al. shows, for example, an automated approach for crack initiation detection by DIC [189]. This approach is however strongly dependent on the available equipment and access in the testing rig, which was not given in the present case.

### 7.2.4 Welding of the specimens

For the Fatigue Crack Growth (FCG) experiments, the specimen for biaxial testing of advanced materials was made of a third generation aluminium-lithium alloy, AA2198-T8. The specimen is illustrated in Figure 7.7. Dimension and strain gauge positions are shown in Figure 7.6.

![Figure 7.6: Drawing of the specimen including the strain gauge positions in X,Y coordinates.](image)

Friction Stir Welding was performed by Airbus Operations GmbH in Bremen on a MTS iStir 5 welding machine. Welding parameters were a downforce of 8.5kN, a welding speed of 500mm/min, a spindle rotation of 1200rpm and 0° tilt angle. The tool used for welding had scroll shoulder with a diameter of 13mm and a pin was tapered from a diameter of 4.9 to 3.9mm. The maximum pin length of 3mm was adjusted automatically to have a distance of 0.2mm from the backing bar.

The specimen thickness in the pocketed areas is 1.6mm, while the thickness of the pad-up is 3.2mm. All specimens, except the fourth, were made of rolled aluminium plates with a thickness of 3.2mm. For the fourth specimen, a 4mm thick aluminium plate was used, and after welding, the specimen was machined on the shoulder surface until it had the same thickness as the other three specimens. While the specimens BIAX1 and BIAX3...
Fatigue of Friction Stir Welded joints under biaxial loading

were welded orthogonally to the rolling direction, specimens BIAX2 and BIAX4 were welded parallel to the rolling direction. This was done to simulate both the transversal and longitudinal welds of a fuselage barrel.

Figure 7.7 shows a photograph of a welded specimen with the coordinate system definition used. The side of the plate where machining was performed and where the shoulder was located during welding is designated as the internal face, and the opposite surface is designated as the external face. The initial crack was in the geometrical centre of the specimen and aligned with the welding direction in this case.

![Photo of a welded specimen with the defined coordinate system.](image)

Figure 7.7: Photo of a welded specimen with the defined coordinate system.

The initial notch was located in the geometric centre of the specimen, but the welding was offset by a distance of half a shoulder diameter. The notch was created by EDM and the crack initial sites were further sharpened with a razor blade. This means that the initial crack was placed between the Thermo Mecanically Affected Zone (TMAZ) and the Heat Affected Zone (HAZ) of the retreating side of the joint, where the material mechanical properties are believed to be the weakest.

**7.2.5 Distortion of the welded specimens**

The commercially available Pontos optical deformation measurement system from GOM mbH was used for measuring the initial shape of all specimens. The measurement system was used slightly outside of the recommended measurement volume due to the
large specimen size, nevertheless a pixel deviation between 0.013 and 0.04 pixels was found during calibration for the different specimens. In this context it should be noted that a value of up to 0.04 pixels is acceptable for good measurement results according to the measurement system manufacturer. A accuracy below 0.1mm was obtained. Furthermore, since all specimens were measured in the same conditions, comparability is guaranteed. This is especially important, since the own weight of the specimens may slightly influence the measured shape, even if in the present case no difficulties have arisen due to this fact. Figure 7.8 shows the specimen BIAX3 prepared for optical distortion measurement. The black and white markers used for the three-dimensional coordinate determination by the stereo vision based system are clearly visible.

![Specimen prepared for the out-of-plane distortion measurement by the stereo vision based system.](image)

**Figure 7.8:** Specimen prepared for the out-of-plane distortion measurement by the stereo vision based system.

The first of the analysed specimens was illuminated by a 2000W halogen studio light. All subsequent specimens were illuminated by a LED based lighting system developed for such measurement tasks. This cold light source does provide a significantly friendlier working atmosphere, without adversely affecting the measurement quality.

Schneider-Kreuznach lenses with a focal length of 50mm and a maximum aperture of f2.8 were used with an aperture of f11 to guarantee a sharp image throughout the whole measurement volume.

The overall distortion was calculated by measuring the distance between two planes parallel to the least squares best fit plane through the centroid which enclose all measured
points according to Section 2.4.2, see Table 7.2. In this way it is possible to compare complex shape distortions quantitatively with low effort. This information should however always be accompanied by a qualitative analysis of the measured shapes.

Table 7.2: Overall distortion of each of the biaxial specimens.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Distortion [mm]</th>
<th>Comment</th>
</tr>
</thead>
<tbody>
<tr>
<td>BIAX1</td>
<td>24.9</td>
<td>rolling orthogonal to the welding direction</td>
</tr>
<tr>
<td>BIAX2</td>
<td>19.4</td>
<td>rolling parallel to the welding direction</td>
</tr>
<tr>
<td>BIAX3</td>
<td>25.0</td>
<td>rolling orthogonal to the welding direction</td>
</tr>
<tr>
<td>BIAX4</td>
<td>32.9</td>
<td>rolling parallel to the welding direction</td>
</tr>
<tr>
<td></td>
<td></td>
<td>(was completely machined on one surface)</td>
</tr>
</tbody>
</table>

Figure 7.9 shows the measured out-of-plane distortion of the specimens. The distorted shapes of the first three specimens are almost identical, while the fourth specimen differs.

Specimens welded orthogonally to the rolling direction seem to have a higher overall distortion after machining. It should be remembered that the specimen BIAX4 has a different shape distortion, which should not be considered for comparison. This is a result of milling the shoulder side of the plate in order to reduce its thickness of 4mm to the same thickness of the other specimens before machining of the pockets, see Figure 7.9(d). Most likely this additional distortion is due to the redistribution of the residual stresses present in the parent material when part of these residual stresses are relaxed.

The specimens distorted due to the welding and machining processes, but after clamping became flat. This means that, even before applying the external loads, initial strains are created on the specimen, which are related to the flattening forces on the distorted specimen. These strains may be significant if the initial distortion is significant, and should therefore be considered in the numerical simulations. Table 7.3 shows the measured strains due to the clamping force after reaching the load level of 4kN. High strains (almost 600µε) are created on the specimen surface due to the initial distortion, and high differences between specimens are observed. In order to be able to compare the measured data with the numerical models that do not model the control of the initial distortion, after measuring the distortion-induced initial strains, all strain gauges were balanced at the initial load of 4kN.
Chapter 7. Fatigue of Friction Stir Welded joints under biaxial loading

Figure 7.9: Out-of-plane distortion measured by the optical method.

Table 7.3: Clamping force induced initial strains (units: $[\mu\varepsilon]$).

<table>
<thead>
<tr>
<th>Specimen</th>
<th>$\varepsilon_{x1}$</th>
<th>$\varepsilon_{x2}$</th>
<th>$\varepsilon_{x3}$</th>
<th>$\varepsilon_{x4}$</th>
<th>$\varepsilon_{x2\text{ ext.}}$</th>
<th>$\varepsilon_{y1}$</th>
<th>$\varepsilon_{y2}$</th>
<th>$\varepsilon_{y3}$</th>
<th>$\varepsilon_{y4}$</th>
<th>$\varepsilon_{y2\text{ ext.}}$</th>
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<tbody>
<tr>
<td>BIAX1</td>
<td>61</td>
<td>216</td>
<td>23</td>
<td>-172</td>
<td>101</td>
<td>609</td>
<td>476</td>
<td>596</td>
<td>270</td>
<td>-93</td>
</tr>
<tr>
<td>BIAX2</td>
<td>15</td>
<td>114</td>
<td>135</td>
<td>-171</td>
<td>95</td>
<td>540</td>
<td>446</td>
<td>489</td>
<td>343</td>
<td>-55</td>
</tr>
<tr>
<td>BIAX3</td>
<td>-49</td>
<td>-119</td>
<td>59</td>
<td>-338</td>
<td>-54</td>
<td>575</td>
<td>498</td>
<td>462</td>
<td>387</td>
<td>-182</td>
</tr>
<tr>
<td>BIAX4</td>
<td>147</td>
<td>-262</td>
<td>204</td>
<td>570</td>
<td>207</td>
<td>-209</td>
<td>-110</td>
<td>-161</td>
<td>-476</td>
<td>491</td>
</tr>
</tbody>
</table>
7.2.6 Loading scenario

Most of the works published concerning fatigue crack propagation in lightweight structures including stiffened panels [21, 248–253] have in common that only unidirectional load is applied to the cracked specimen. The present work seeks to extend the knowledge about fatigue crack propagation in integral structures to a biaxial stress field. It focuses on the biaxial testing of advanced materials, in this case a third generation aluminium lithium alloy, AA2198-T8, being considered for application in aeronautical structures.

Advanced analyses of the behaviour of those structures should include biaxial fatigue testing, since real-life service loading conditions are typically not uniaxial. The aircraft fuselage shell should be considered as a pressure vessel additionally subjected to external loads. Therefore significant planar loads in longitudinal and circumferential directions are to be expected.

Due to manufacturing reasons, aluminium panels are much longer in the rolling direction than in the perpendicular direction. Fuselage panels are manufactured, with very few exceptions, with the rolling direction oriented in the aircraft flight direction, since in this way longer panels can be produced and the number of circumferential joints is therefore minimised.

The specimens used in the present work, with the weld seam orthogonal to the rolling direction, simulate circumferential joints, the specimens welded in rolling direction simulate longitudinal joints. The load ratio 1:1 simulates upper fuselage conditions under the effect of pressurisation and bending loads. Load-ratio 1:2 represents lower fuselage conditions not tested in the current work.

The maximum applied stress of 100MPa was defined for research purposes, since in general the maximum stress level for 1.6mm skin pockets is below 120MPa, and in the joint areas the stresses are usually much lower than that [254]. This values may be derived from the stress present in a not reinforced cylindrical shell when subjected to a pressure difference between the inner and outer surface. These values may be used as a good first approximation for the loads present in a typical aircraft fuselage shell.

7.3 Results

7.3.1 Static loading

During the whole clamping process, strain was acquired using Hottinger Baldwin Messtechnik (HBM) strain gauges with a nominal resistance of 350Ω and a gauge length of 6 and
3mm. Afterwards the specimens were loaded statically in order to check the load transfer and verify the numerical model calculated strains. Another objective was to ensure that all specimens had similar initial conditions. Various load combinations between the X and Y axes were investigated.

Different approaches may be used for measuring strains on biaxial specimens. Ramault et al. compared electronic speckle pattern interferometry, DIC and the traditional strain gauges [255]. While for small specimens, it may be true that DIC gives the most comprehensive information of the strain distribution, in the current case, in view of the larger specimen size and the difficulty to access it inside of the testing rig, led to the decision that strain gauges were a viable alternative. Since strain gauges are able to measure local strains very accurately only in one direction, the sensor locations shown in Figure 7.6 were chosen in order to acquire the required distribution for the present case.

Static loading of the specimens was performed in order to verify the numerical models, and in order to guarantee that all specimens had similar initial conditions. The stress distribution in the crack growth area was verified. Various load combinations between the X and Y axes were performed. In the first and second variants, only the Y and X axes were loaded up to the maximum load of 40kN, leaving the other axis at the 4kN minimum load. In the third variant, the load was applied simultaneously on both axes up to the maximum fatigue load of 40kN and in the forth variant, only half of the load was applied to the X axis, while the Y axis was fully loaded. Only the last variant is shown in this paper.

Before the static loading experiment, the specimens were loaded up to 10kN three times and afterwards the strain gauges were set to zero at a load of 4kN. At this load the specimens were flat inside the testing rig. Figure 7.10 shows the load cycles applied to the specimen during static loading.
The strain introduced in the specimen by the application of the forces from loading scenario "D" discussed before is represented in Figures 7.11 and 7.12.
Chapter 7. Fatigue of Friction Stir Welded joints under biaxial loading

Figure 7.11: Strain depending on the applied force - X direction.

This static loading data can be used for verification of the FEM model. Strain at the individual points should be similar in order to ensure that the reality is sufficiently well simulated. Strain readings were linear with the increasing load, further validating the clamping setup and general quality of the testing setup. Additionally this measured data demonstrated that all specimens were tested under comparable loading conditions and that the clamping of each of the specimens led to similar initial load levels in the specimens. The stress in the central testing area was shown to be uniform and represents the required stress biaxiality.
7.3.2 Cyclic loading

Specimens welded orthogonally to the rolling direction simulate circumferential joints, specimens welded in rolling direction simulate longitudinal joints. The load ratio 1:1 simulates upper fuselage conditions under the effect of pressurisation and bending loads. The maximum applied stress of 100MPa was defined for research purposes, since in general the maximum stress level for 1.6mm skin pockets is below 120MPa, and in the joint areas the stresses are normally much lower than that.

Therefore the fatigue load was defined by sinusoidal loads with a maximum load of 40kN and a load ratio R=0.1. The loading in both directions, X and Y was applied without phase-shift in the current case.

Figure 7.13 shows the evolution of the strain gauge values measured at the load [X = 40kN; Y = 40kN] as a function of the number of fatigue cycles. These values were
recorded at each crack length measurement step.

Figure 7.13: Evolution of the strain gauge signals at maximum load.

The initial strain measured on all specimens is very similar, which is a good sign for the quality and repeatability of the testing setup. Also the tendency of the strain evolution as a function of number of cycles is comparable, having similar tendencies. The main differences arise when the strain gauges reach the end of their useful measuring capacity. This means that the crack grew below some of the strain gauges, effectively eliminating the capability for measuring surface strain. There does not seem to be any significant dependency on the rolling or welding direction in these measurements. The usefulness of the obtained results is therefore mostly limited to the verification of comparability between specimens at the beginning of the test, additional quality assurance during the experiment and for comparison to FEM models.

Figure 7.14 shows the crack length vs. numbers of cycles measured during fatigue loading of all specimens. All crack length measurements were taken with the maximum load of 40kN applied to the Y-axis and the minimum load of 4kN applied to the X-axis. In
order to be able to compare the results more accurately, the results are shown for an initial notch $a_{\text{initial}} = 22 \text{mm}$, which means that the initial cycles for developing a crack from the initial notch were not considered.

![Graphs showing crack growth](image)

**Figure 7.14:** Crack growth during the fatigue loading test.

Figure 7.15 and 7.16 show the crack-path measured during the fatigue crack growth experiment.

The difference between the results of a crack growing in parallel and perpendicular directions to the rolling directions is apparent. As can easily be verified, the specimens welded orthogonally to the rolling direction had a less symmetrical fatigue crack growth behaviour than the specimens welded parallel to this direction.

From Figure 7.15 it is clearly visible that the crack growth was slower on the side where the crack entered the welded material, which seems to be the case for other materials [21], and was also partially verified for AA2198 [256, 257]. In the case of the specimens results presented by Irving et al., residual stresses present in the fatigue crack growth specimens may have affected the results, but since the specimen is comparable to the cruciform specimens tested in the present work, a similar behaviour may be expected. The results
shown in Figure 7.15 were obtained from specimen COI-AR-FSW86 discussed in Section 5. Two base material specimens in rolling direction and two in transversal direction were tested according to ASTM E647. Additionally specimens with the initial notch in the weld nugget and in the transition zone on the retreating side were tested according to Figure 7.18. As can be seen, crack growth is slightly faster in rolling direction than in transversal direction in the base material, which helps to explain why the crack had a tendency to follow the rolling direction of the specimens. The same effect has also been noticed by Cavalieri and Santis for a higher stress ratio [258]. Furthermore the slower crack growth speed in the welded material helps to understand why the crack growth was not symmetric in the case of the biaxial specimens welded orthogonal to the rolling direction, preventing the crack of entering the weld centre line.
Figure 7.16: Crack growth path as measured by the optical system - welding parallel to the rolling direction.
Chapter 7. Fatigue of Friction Stir Welded joints under biaxial loading

Figure 7.17: Fatigue crack growth rate comparison of AA2198 base material in rolling and transversal directions.

Figure 7.18: Location of the FCG specimens on plate COI-AR-FSW86.
7.3.3 Secondary cracks

Secondary cracks developed in two corners on both specimens welded parallel to the rolling direction. The $K_t$ calculated for this corner was 3.2 [203]. Applying a remote stress of 100MPa, the local stress is therefore expected to be around 320MPa, which is above the fatigue limit for $10^6$ cycles around 310MPa [259]. The location of the secondary crack is shown in Figure 7.19. The fact that only this kind of specimens was affected by secondary cracks most likely is related to their longer total fatigue life when compared to the specimens welded perpendicular to the rolling direction, which can be verified in Figure 7.14.

![Figure 7.19: Location of the secondary cracks, shown on a dummy specimen.](image)

On the specimen BIAx2, secondary cracks were detected on both sides of one of the arms perpendicular to the welding direction, being the secondary cracks parallel to the primary crack. The first crack appeared around 148k cycles reaching a final length of 17mm, the second secondary crack appeared around 276k cycles reaching a final length of 13.5mm. They were held stationary with drilled holes and mechanical clamping devices in order to proceed with the experiment.

On specimen BIAx4, at 271k cycles the first secondary crack was detected, which at the end of the experiment reached 12.5mm. This crack was successfully stopped by mechanical clamps. At 279k cycles a second secondary crack was detected, reaching 24mm at the end of the experiment. Even with clamps applied and a small hole drilled at its crack tip, this crack could not be completely stopped, but sufficiently retarded to finish the experiment. Both cracks grew on the arm parallel to the welding direction.
Figure 7.20 shows the mechanical clamps used for stopping the secondary cracks after a hole was drilled at the crack tips.

The relatively small secondary cracks and the fact that they were far away from the primary crack tips and additionally that they were mechanically clamped, leads to the conclusion that they did not influence the primary crack growth behaviour. No change in behaviour could be detected in the crack growth rate or direction of the primary crack due to the existence of the secondary cracks.

7.4 Conclusions

In this chapter the specimen design and experimental setup for conducting biaxial load testing of fatigue crack growth in thin-walled panels was presented. Realistic test conditions could be reproduced. Measurement of strain distributions and crack lengths are also discussed.

The equipment used in the present case has more capabilities than necessary for the present application. From the six cylinders with 1000kN load capacity each, only four were used at a maximum load of 40kN, which is only 4% of the loading capacity. Therefore some additional complexity was found during the experiments, such as the electrical interference control and little space for accessing and inspecting the specimens.

The specimen design was based on the review of existing designs and a finite element analysis, leading to a large enough round radius at the intersection of the loading arms, which should be able to prevent secondary cracks from initiating at those locations.
It was verified that the crack had a high tendency of resisting entering the welded material and that the preferred crack growth direction in the present case was in rolling direction which helps to explain why the crack growth behaviour of specimens tested in the rolling and transversal directions was different. The crack growth was symmetric in specimens welded parallel to the rolling direction. Furthermore, specimens welded orthogonally to the welding direction, with the crack growing parallel to the weld, exhibited a lower fatigue life than specimens welded parallel to the rolling direction.

It was verified that the load in the crack growth area was evenly enough distributed so that the crack growth data obtained remain valid.
Chapter 8

In situ Stress Intensity Factor determination system

A methodology for in situ Stress Intensity Factor (SIF) determination that can be used for the analysis of cracked structures is described. In the present chapter, an existing algorithm was enhanced, among others with the ability of automatically detecting the crack tip. The proposed method is applied to the case of a central cracked plate, subjected to uniaxial tension. Three steps are used for the SIF determination. First the strain field around the crack tip is acquired using a digital image correlation based optical technique. Secondly, the stresses are calculated based on the equations from the theory of elasticity. In the third step an over determined system of equations is solved including the stress field around the crack tip and the stress intensity factor among others as variables. A comparison of the obtained results was performed with results obtained using the Dual Boundary Element Method together with the J-Integral method for SIF determination. A good agreement can be noticed for both the stress distribution around the crack tip and for the SIF calculated based on these stresses, proving thus the ability to measure the SIF in situ with inclusion of possible residual stress effects.
8.1 Experimental Stress Intensity Factor determination

Modern lightweight structures are often based on damage tolerance design principles, which means that the structure has to withstand the existence of cracks up to the next routine maintenance inspection or up to its defined economical end of life. The maintenance intervals have to be chosen in a way that guarantees a secure usage of the structure, which often leads to high inspection costs.

A better knowledge of the real effect of existing cracks could lead to a better planning of the maintenance operations after a crack has been found in a structure. The possibility to measure the real SIF of a crack in situ is therefore of high interest.

In Linear Elastic Fracture Mechanics (LEFM), the SIF concept is used for damage tolerance considerations. For complex geometries containing residual stresses however, it is difficult to precisely define the SIF. For this reason, a method was developed, which allows surface in situ measurement of the SIF, including for complex shells containing residual stresses.

Since the ability to tolerate a substantial amount of damage is a requirement for modern lightweight structures, it has become increasingly important to develop methodologies to predict failure in fatigue damaged structures. The damage tolerant philosophy must ensure the continued safe operation of structures, which means that a structure is supposed to sustain cracks safely until it is repaired or its service life has ended. Strength assessment of structures is necessary for their in-service inspection, repair, rehabilitation, and health monitoring. The damage tolerance analysis should provide information about the effect of cracks on the strength of structures. Damage tolerance analyses can be performed using LEFM concepts where the SIF is a fundamental parameter. Fracture mechanics in conjunction with crack growth laws, e.g. [260], is widely used to analyse and predict crack growth and fracture behaviour of structural components. To study crack growth and to evaluate the remaining life of a certain structural component, rigorous numerical analyses have to be performed to compute SIFs.

Structures can suffer fatigue damage throughout their service life leading to constant changes in geometry. The assumptions made during the design phase are therefore constantly changed, and numerical analyses previously performed no longer accurately show the stress distribution at critical locations. With the ability to monitor the fatigue process in situ, Non-destructive testing (NDT) methods are of critical importance for structural integrity evaluation and failure prevention of engineering components in service. The development of experimental techniques to obtain the SIF in real structures is therefore of high interest.
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The technique presented in this work can provide a powerful experimental tool to investigate localised inhomogeneous damage and to analyse complex fatigue processes. A better fatigue-life estimation becomes feasible and reduced maintenance costs may be expected as a result.

Concurrently with the present work, Catallanoti et al. used an energetic method, the J-Integral, for SIF determination using DIC [261]. In the present case the strain field in the vicinity of a crack is used. The use of the strain field instead of the displacement field poses challenges due to the higher noise associated to this kind of measurements, but has the advantage of automatically eliminating the rigid body motion component of the measured data without the need for external reference points or more complex systems of equations. As long as the noise can be controlled, this leads to advantages in real structures, where only a small section of a structure has to be monitored, without having to consider rigid body motions in this section.

In order to evaluate the accuracy of the proposed algorithm for SIF determination, the results were compared with the numerical ones obtained using the Dual Boundary Element Method (DBEM).

### 8.2 Experimental approach

An optical strain and deformation measurement system based on digital image correlation is used to measure the strain distribution around a fatigue crack tip. Based on the obtained measurement results, the SIF can be calculated by fitting the experimentally obtained results in the analytical stress expression near the crack tip, in the form of a series expansion with different number of terms. The experiments were performed on a servo-hydraulic MTS 312.31 testing machine using a 250kN load cell. Ambient temperature was 24°C during the whole experiment. Figure 8.1 shows the experimental setup.
Figure 8.1: Experimental setup for measurement of the strain around the crack tip.

Figure 8.2 shows a scheme of the experimental setup. The camera points to the area around the crack tip, where the speckle pattern was applied. Strain is calculated by measuring the relative displacement of neighbouring regions inside the pattern area. The stochastic pattern helps in the identification of different regions on the specimen, since the specimen material does not provide enough detectable detail without this applied pattern.
8.2.1 Optical strain and deformation measurement system

For the images taken in the unloaded state, a force of only 200N was considered, in order to allow the specimen to stay motionless during the measurement which was performed under load-control on the servo-hydraulic system. The loaded state was measured at the maximum load of the fatigue test, namely $F_{\text{max}} = 21760\text{N}$. The crack extension was measured using a travelling optical microscope attached to a Mitutoyo digital scale with a 1/100mm resolution.

A portable GOM mbH Aramis 6.0.2 workstation was used for measurement of the strain field around the crack tip. This system is based on digital image correlation, and therefore information about recognisable facets has to be given to the system. The GOM mbH 2M hardware (CCD based grayscale camera with 2 megapixel resolution) was used with 50mm focal length Schneider-Kreuznach lenses with a maximum aperture f/2.8. Two 18W white fluorescent lamps at 6400K were used for illumination of the specimen surface.

The specimen is painted with a stochastic pattern necessary for the digital image correlation operation. A fine black dotted pattern is applied to the white-grounded surface for better contrast. The quality of the results proves to be high when such a prepared surface is used. Figure 8.3 shows the stochastic pattern applied to the specimen surface, which is used for strain calculation. This image is also used for manual crack tip detection in order to be able to verify the automatically detected crack tip based on the strain field.

![Stochastic pattern applied to the specimen surface for strain calculation; the crack can be recognized due to the maximum load that was applied for this image.](image)

**Figure 8.3:** Stochastic pattern applied to the specimen surface for strain calculation; the crack can be recognized due to the maximum load that was applied for this image.
Since strains are measured by this system, effectively eliminating any possible rigid body motion or rotation during the experiments, these have to be converted into stresses before they may be applied in the algorithms. This additional step is possible since in the present case only thin-walled structures are analysed, and therefore the stresses may be calculated using Hooke’s law for plane stress, see Equation 8.1. Note that the number “100” in these equations results from the conversion of the strain measured in percentage by the available system to non-dimensional strain.

\[
\sigma_y = \frac{E}{(1 - \nu^2)} (\varepsilon - \nu \varepsilon_y) \frac{1}{100} \\
\sigma_x = \frac{E}{(1 - \nu^2)} (\varepsilon_y - \nu \varepsilon) \frac{1}{100} 
\]

(8.1)

8.2.2 Measurement results

The painted specimen is virtually divided into facets with a size of 7×7 pixels and a facet step of 5 pixels. This means that every 5 pixels a new 7 pixel long facet starts, and overlap between facets is 2 pixels, corresponding to 29% in X and Y directions. Due to the small distance from the lenses to the specimen, an area of around 20×15mm², corresponding to 324×246 facets, was measured. Strain is calculated by measuring the deformation of a facet in relation to its neighbouring facets. In order to obtain a smooth measurement, 19×19 facets were chosen for strain measurement. This is equivalent to a gauge length of about 1.21mm in traditional measuring instruments. The validity quote was chosen to be 55%, which means that at least 55% of the 19×19 facets have to exist in order to allow the calculation of strain for a certain facet. Figure 8.4 shows the obtained strain distribution around the crack tip in the direction perpendicular to the crack plane used for calculation of the SIF.

![Figure 8.4: Smoothed strain data in Y direction; \( \varepsilon_y \) units are \( \mu \varepsilon \). The crack tip is located at the centre of coordinates.](image-url)
The area effectively used for analysis is shown in Figure 8.5.

![Figure 8.5: Area used for analysis.](image)

The “start distance” of the quadratic “area of interest” should be chosen as near as possible to the crack tip, but without being affected by the measurement errors of the system, incapable of measuring strain at the crack tip. The “end distance” should be chosen high, but is limited by the area of influence of the crack. A very high “end distance” will not influence the results, but only increase noise. The “grid step” defines how many data points are used for calculation. The minimum number of data points is defined by the variables to be calculated by the overdeterministic algorithm. A higher number of data points leads to better calculation results, but more data point areas are normally also more affected by noise in the measured data. In the present case the “grid step” was chosen as being the smallest value that was still acceptable in terms of computational effort. Table 8.1 shows the parameters that were found to lead to the most reliable results.

**Table 8.1: Parameters of the area used for SIF determination.**

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>start distance</td>
<td>0.1mm</td>
</tr>
<tr>
<td>end distance</td>
<td>7.5mm</td>
</tr>
<tr>
<td>grid step</td>
<td>0.05</td>
</tr>
</tbody>
</table>
8.2.3 Automatic crack tip detection

In the authors own experience, using digital image processing, the crack tip is very difficult to detect accurately, especially if the acquired images do not provide sufficient quality or resolution [247]. The acquired strain field was therefore used for automatic crack tip detection. First the gradient of the strain field perpendicular to the crack direction was calculated. Then the variations in this gradient were used for crack tip detection. Therefore the variation was compared to an artificially defined threshold of 12 times the average variation between measurement points. Although this method is prone to errors due to the noise in the strain measurement, good agreement with optical crack detection methods was found. Furthermore, this method allows the detection of the crack tip, even if the crack tip is not visible on the specimen surface due to the applied white paint with stochastic pattern.

Figure 8.6 shows the automatically detected crack tip. The algorithm detects the crack tip 0.3mm earlier than the optical method, which may be due to the plastic zone ahead of the crack tip which is detected by stress field based systems. Optical measurements are used as a benchmark against which the automatic detection process is tested.

![Image](image.png)

**Figure 8.6:** Very high magnification of an automatic crack tip detection result.

In an attempt do understand which detected crack tip is the best approximation for the real crack tip of the specimen, the SIF is calculated using $n = 3$ to $n = 18$ terms of the series expansion shown later on. If the crack tip location is correct, the series is expected to converge. The automatically detected crack tip shown in Figure 8.6 is the baseline, and small corrections are applied to this location, see Figure 8.7.
(a) area of interest: 0.15 mm to 7.5 mm after assumed crack tip

(b) area of interest: 0.2 mm to 7.5 mm after assumed crack tip

(c) area of interest: 0.3 mm to 7.5 mm after assumed crack tip

(d) area of interest: 0.5 mm to 7.5 mm after assumed crack tip

(e) area of interest: 1.0 mm to 7.5 mm after assumed crack tip

(f) area of interest: 1.5 mm to 7.5 mm after assumed crack tip

**Figure 8.7:** SIF calculated for different “detected” crack tip locations for n=3 to n=18 terms of the series expansion. The Figures refer to the case of $a_1=14$ mm.
It can be verified that independently of the distance of the analysed area to the assumed crack tip location, a correction between +0.3 and +0.4mm leads to convergence of the series expansion for the case of the left crack tip ($a_1$) and between -0.3 and -0.4mm for the right crack tip ($a_2$). Anyway, higher distances to the crack tip tend to lead to less accurate results when compared to DBEM or analytical results. Figure 8.8 shows the same results for other crack lengths and a area of interest starting at 0.15mm from the assumed crack tip.

Figure 8.8: SIF calculated for different “detected” crack tip locations for n=3 to n=18 terms of the series expansion.
It is shown that the series expansion does always converge for a slightly corrected assumed crack tip. This finding leads to the conclusion that the automatically detected crack tip should be corrected by approximately 0.3 mm, in direction opposite to the crack growth direction in most cases, in order to lead to a convergence in the series expansion. This correction may be related to a combination of the plastic zone ahead of the crack tip and the visible surface crack extension. Due to the not straight shape of the crack front, the visible surface crack is shorter than the average crack length though the thickness of the specimen. The correct assumed crack tip location is therefore found by checking for convergence of the series expansion. This information is used for the definition of the correct crack tip for SIF calculation. The algorithm automatically defines the necessary correction for the assumed crack tip location slightly until convergence of the series is obtained.

Applying the same procedure in the direction perpendicular to the weld simultaneously does however lead to some problems, in most cases $K_I$ and $K_{II}$ do not converge simultaneously since they were calculated by different strain fields $\varepsilon_y$ and $\varepsilon_x$ respectively. Anyway, it was found that the $K_{II}$ values converge for 13 terms even if only the position in crack direction is corrected. Afterwards the values tend to be meaningless, since the quality of the measurements of $\varepsilon_x$ does not allow to use more terms. The average value of the converged terms is used as the final result for $K_I$ and $K_{II}$.

### 8.2.4 Overdetermined mixed mode SIF calculation

Knowing the stress field around the crack tip, it is possible to apply an overdeterministic SIF calculation method as it was shown in [213] for example.

For the plane stress problem of a homogeneous isotropic solid, in the absence of body forces, the global field equations for the stress components in the vicinity of a straight front crack under mode I conditions can be written as shown by [262]:

\[
\sigma_X = \frac{K}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left( 1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) + \sigma_{0x} + \sum_{n=3}^{\infty} \left( A_n \frac{n}{2} \right) r^{\frac{n}{2} - 1} \left\{ \left[ \frac{2 + (-1)^n + n}{2} \right] \cos \left( \frac{n}{2} - 1 \right) \theta - \left( \frac{n}{2} - 1 \right) \cos \left( \frac{n}{2} - 3 \right) \theta \right\} 
\]

\[
\sigma_Y = \frac{K}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left( 1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) + \sum_{n=3}^{\infty} \left( A_n \frac{n}{2} \right) r^{\frac{n}{2} - 1} \left\{ \left[ \frac{2 - (-1)^n - n}{2} \right] \cos \left( \frac{n}{2} - 1 \right) \theta + \left( \frac{n}{2} - 1 \right) \cos \left( \frac{n}{2} - 3 \right) \theta \right\} 
\]

\[
\tau_{XY} = \frac{K}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \cos \frac{\theta}{2} \cos \frac{3\theta}{2} - \sum_{n=3}^{\infty} \left( A_n \frac{n}{2} \right) r^{\frac{n}{2} - 1} \left\{ \left[ (-1)^n + n \right] \sin \left( \frac{n}{2} - 1 \right) \theta + \left( \frac{n}{2} - 1 \right) \sin \left( \frac{n}{2} - 3 \right) \theta \right\} 
\]
The same equations for the stress components in the vicinity of a straight front crack under mixed mode conditions may be written as shown by Sakaue et al. [263] in equations 8.5 to 8.7.

\[
\sigma_X = \frac{K_I}{2\sqrt{2\pi r}} \left[ \frac{3}{2} \cos \left( \frac{1}{2} \theta \right) + \frac{1}{2} \cos \left( \frac{5}{2} \theta \right) \right] + \frac{7T}{8} \tag{8.5}
\]

\[
- \frac{K_{II}^n}{2\sqrt{2\pi r}} \left[ \frac{7}{2} \sin \left( \frac{1}{2} \theta \right) + \frac{1}{2} \sin \left( \frac{5}{2} \theta \right) \right]
\]

\[
+ \sum_{n=3}^{\infty} A_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(2 - (-1)^n + \frac{n}{2}\right) \cos \left(\frac{n}{2} \theta\right) - \left(\frac{n}{2} - 1\right) \cos \left(\frac{n}{2} - 3\right) \theta \right]
\]

\[
- \sum_{n=3}^{\infty} B_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(2 - (-1)^n + \frac{n}{2}\right) \sin \left(\frac{n}{2} \theta\right) - \left(\frac{n}{2} - 1\right) \sin \left(\frac{n}{2} - 3\right) \theta \right]
\]

\[
\sigma_Y = \frac{K_I}{2\sqrt{2\pi r}} \left[ \frac{5}{2} \cos \left( \frac{1}{2} \theta \right) - \frac{1}{2} \cos \left( \frac{5}{2} \theta \right) \right] \tag{8.6}
\]

\[
- \frac{K_{II}^n}{2\sqrt{2\pi r}} \left[ \frac{1}{2} \sin \left( \frac{1}{2} \theta \right) + \frac{1}{2} \sin \left( \frac{5}{2} \theta \right) \right]
\]

\[
+ \sum_{n=2}^{\infty} A_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(2 - (-1)^n - \frac{n}{2}\right) \cos \left(\frac{n}{2} \theta\right) + \left(\frac{n}{2} - 1\right) \cos \left(\frac{n}{2} - 3\right) \theta \right]
\]

\[
- \sum_{n=2}^{\infty} B_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(2 - (-1)^n - \frac{n}{2}\right) \sin \left(\frac{n}{2} \theta\right) + \left(\frac{n}{2} - 1\right) \sin \left(\frac{n}{2} - 3\right) \theta \right]
\]

\[
\tau_{XY} = \frac{K_I}{2\sqrt{2\pi r}} \left[ \frac{1}{2} \sin \left( \frac{1}{2} \theta \right) + \frac{1}{2} \sin \left( \frac{5}{2} \theta \right) \right] \tag{8.7}
\]

\[
- \frac{K_{II}^n}{2\sqrt{2\pi r}} \left[ \frac{3}{2} \cos \left( \frac{1}{2} \theta \right) + \frac{1}{2} \cos \left( \frac{5}{2} \theta \right) \right]
\]

\[
+ \sum_{n=2}^{\infty} A_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(-1\right)^n - \frac{n}{2}\right) \sin \left(\frac{n}{2} \theta\right) + \left(\frac{n}{2} - 1\right) \sin \left(\frac{n}{2} - 3\right) \theta \right]
\]

\[
- \sum_{n=2}^{\infty} B_n \frac{n}{2} r^{\frac{n}{2}-1} \left[ \left(-1\right)^n - \frac{n}{2}\right) \cos \left(\frac{n}{2} \theta\right) - \left(\frac{n}{2} - 1\right) \cos \left(\frac{n}{2} - 3\right) \theta \right]
\]

In the case of a mixed mode scenario, the use of the \(\sigma_Y\) stresses (perpendicular to the crack face) for \(K_I\) determination is recommended, and the use of \(\sigma_X\) stresses is recommended (parallel to the crack growth direction) for \(K_{II}\) determination, since these stresses are more sensitive to the respective stress intensity factors.

The origin of the cartesian (x, y) and polar (r, \(\theta\)) coordinate systems is defined at the crack tip. According to Lopez-Crespo et al. [214], a low number of terms and a high number of data points is suggested for an accurate SIF calculation. In the present case it is found that at least 12 terms should be used, but up to 18 terms no further variation is observed in the calculated results, see for example Figure 8.8. The number of data points used for calculation was chosen complying with the overdeterministic requirement.
of obtaining the stress components on more points than required by the series expansion, but was also limited by the time necessary for calculation.

From the equations above, it can be verified that all three exhibit linear dependence with the coefficients \( K_f, K_{II}, \sigma_0, A_3, \ldots, A_n \). This means that a linear overdeterministic algorithm can be used to obtain these unknown coefficients, provided the values of the stresses are obtained at different points of coordinates \((r, \theta)\). Note that in the above equations, the T-stress may be determined by the relation \( A_2 = \frac{T}{T} \). The coefficient \( A_2 \) does not appear in the series corresponding to \( \sigma_Y \), but appears in the series corresponding to \( \sigma_X \).

A scheme of the complete algorithm necessary for SIF determination can be seen in Figure 8.9.

![Figure 8.9: Algorithm developed for SIF determination.](image)

The Matlab implementation of this code can be found in Annex C.
8.3 Application example

A pre-cracked specimen with a central notch according to ASTM E647 [177] with a width of 80mm was selected for this experiment due to its size, which is large enough for a good measurement area and small enough to maintain the fatigue loads relatively low. The specimen thickness was 4mm and the 10mm long initial notch was machined by spark erosion (EDM). A drawing of this specimen is shown in Figure 8.10(a).

![MT specimen](image)

(a) general dimensions. (b) detail of the straight crack.

Figure 8.10: MT specimen.

One advantage of this type of specimen is the fact that it has a geometry which allows the analysis of symmetric and non-symmetric cracks.

For calculation purposes, the strain field is considered in a small quadratic area around the crack tip only, starting 0.45mm after the crack tip and extending for 7.05mm away from the tip. In this area, the strain values are read on a grid with 0.05mm spacing, which guarantees a good degree of precision.

Table 8.2 shows the parameters used for fatigue loading, and Figure 8.10(b) shows a scheme of the studied crack geometry.
Table 8.2: Fatigue loading parameters.

<table>
<thead>
<tr>
<th>Property</th>
<th>Value</th>
</tr>
</thead>
<tbody>
<tr>
<td>$F_{\text{mean}}$</td>
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</tr>
<tr>
<td>$F_{\text{amplitude}}$</td>
<td>9792N</td>
</tr>
<tr>
<td>load ratio (R)</td>
<td>0.1</td>
</tr>
<tr>
<td>frequency</td>
<td>4Hz</td>
</tr>
</tbody>
</table>

8.3.1 Numerical SIF determination

Linear elastic fracture mechanics can be used for the analysis of crack behaviour in damage tolerance. The fundamental postulate of LEFM is that the crack behaviour is determined solely by the values of the stress intensity factor, parameter which depends on the applied load and the geometry of the cracked structure.

Numerical methods should be primarily used for the stress analysis of engineering structures because of their complex geometry. When studying crack growth problems, the need for continuous re-meshing is a practical disadvantage of the finite element method (FEM).

The Boundary Element Method (BEM) is better suited for the incremental analysis of crack growth problems. An advantage of using the BEM method is the ability to easily model a great variety of cracks without the need for re-meshing of the model, as it would be necessary when using the finite element method. The main disadvantage of this method is related to difficulties of applying it to more complex structures.

The solution of general crack problems cannot be achieved with the direct application of the standard BEM, because the coincidence of the crack boundaries causes an ill-posed problem. For a pair of coincident source points on the crack boundaries, the algebraic equations relative to one of the points are identical with the algebraic equations relative to the opposite point, since the same boundary integral equation is applied at both coincident source points, with the same integration path, around the whole boundary of the problem. Among the techniques devised to overcome this difficulty, the most general are the sub-regions method and the dual boundary element method.

The DBEM introduces two independent boundary integral equations, with the displacement equation used for collocation on one of the crack surfaces and the traction equation used for collocation on the opposite crack surface. Consequently, general mixed-mode crack problems can be solved in a single-region boundary element formulation, with both crack surfaces meshed with the DBEM.
The stress intensity factor for the numerical verification was therefore calculated by the DBEM, using the J-integral method. The code “Cracker” [264] was used for this purpose, since it has implemented a routine capable of predicting the crack growth path, which allows the validation of crack growth path predictions performed based on the optical strain measurements.

Although this specimen geometry is affected by the stress acting parallel to the crack (T-stress) [265], in the present work this does not interfere with the SIF determination, since using the overdeterministic approach shown in this work, the T-stress component can be intrinsically calculated by the algorithm without further information.

### 8.3.2 Analytical SIF determination

Since the M(T) style specimen has a simple geometry, the SIF for the mode I case can be easily calculated by equation 8.8 for a finite width plate with a central through crack [266]. \( W \) denotes the plate width and \( a \) denotes half crack length. \( \sigma_{rem} \) is the remotely applied stress.

\[
K_I = \sqrt{\sec \frac{\pi a}{W}}\sigma_{rem}\sqrt{\pi a}
\]  \hspace{1cm} (8.8)

This equation leads to an approximate solution for \( K_I \) in the present case for additional validation of the experimental results.

### 8.3.3 Comparison of numerical, analytical and experimental results

Figure 8.11 shows a comparison of experimental and numerical results of the strain near the crack tip for \( \theta = 0^o \) in front of the crack tip. An excellent agreement between the experimental and numerical result was found. This means that the DIC based strain measurement system leads to good results. Problems may arise in the exact determination of the crack tip, leading to an offset in the represented strain distribution.
Chapter 8. *In situ SIF determination system*

For this case, only the stress field at the right side crack was determined experimentally and the stress intensity factor was compared to numerical results. Only mode I was considered, since the crack grows essentially perpendicularly to the loading direction. A comparison of the experimentally obtained SIF using DIC and the numerically determined SIF using the DBEM was made. The results may be seen in Table 8.3. The analytical result was considered as reference value for the calculation of the relative difference.

The strain field around the crack tip can be used in the present case to verify that the crack tip was correctly identified by the optical strain measurement technique. Nevertheless, the main problem was the noise found in the images taken for digital image correlation.

### Table 8.3: Results obtained with this SIF determination process.

<table>
<thead>
<tr>
<th>length [mm]</th>
<th>DIC $K_I$ [MPa√mm]</th>
<th>DIC $K_{II}$ [MPa√mm]</th>
<th>analytical $K_I$ [MPa√mm]</th>
<th>DBEM $K_I$ [MPa√mm]</th>
<th>difference [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>a1</td>
<td>14.02</td>
<td>518</td>
<td>-18</td>
<td>531</td>
<td>470</td>
</tr>
<tr>
<td>a2</td>
<td>14.50</td>
<td>533</td>
<td>2</td>
<td>540</td>
<td>470</td>
</tr>
<tr>
<td>a1</td>
<td>17.80</td>
<td>724</td>
<td>-31</td>
<td>598</td>
<td>550</td>
</tr>
<tr>
<td>a2</td>
<td>17.60</td>
<td>684</td>
<td>0</td>
<td>595</td>
<td>550</td>
</tr>
<tr>
<td>a1</td>
<td>25.20</td>
<td>967</td>
<td>-22</td>
<td>712</td>
<td>748</td>
</tr>
<tr>
<td>a2</td>
<td>23.96</td>
<td>939</td>
<td>1</td>
<td>694</td>
<td>761</td>
</tr>
</tbody>
</table>

The mode II SIF, $K_{II}$, is expected to be zero in this case, as long as the loading conditions have been well defined in the experimental setup.
8.4 Conclusions

As it can be seen, similar values were obtained for the measured and calculated strain fields, which raises the confidence in the optical strain measurement system. Additionally, the experimental result for the SIF in mode I is promising, giving confidence that this technique can be applied to real life structures in order to assess their structural integrities. According to Pan [147], as long as a 3D DIC setup is used, out-of-plane strains, such as in plate curvature, may also be measured.

A high image quality is crucial for obtaining good results. In the present work this challenge was successfully overcome by the algorithm, and it was shown that even in the worst case, the measured SIF differed by just over 30% from the calculated value.

A new procedure for processing the experimentally obtained strain values was proposed in this work. The strains measured with the GOM mbH Aramis system were first converted into stresses, using the well known equations of the theory of elasticity. Then, equations of the stress field around the crack tip, written as series development with seven terms, were used to fit the experimental data, obtaining thus an overdetermined system of equations in which the coefficients of the series expansion are the unknowns.

The overdeterministic algorithm, a numerical procedure for solving such systems, was used to solve the system and to obtain the stress intensity factor, which is the coefficient of the first term of the expansion.

The analytical solution of the SIF validated the methodology. A reasonable difference was achieved, proving thus the reliability of the proposed method as long as the strain measurements are of good quality.

Due to its flexibility, this method can be applied for in situ fracture mechanics researches on real structures, for which a laboratory model is difficult or even impossible to conceive.
Chapter 9

Friction Stir Welding for assembly of aircraft fuselage barrels

While studying the different aspects of manufacturing lightweight aluminium structures is certainly interesting, a widely enhanced knowledge of these concepts comes from their industrial application in the aeronautical sector. In this context, an internship in the Manufacturing Research and Technology (R&T) department of Airbus Operations GmbH was performed.

The main goal of the tasks developed in the course of this internship, was to study the applicability of FSW at Major Component Assembly (MCA) level in future metallic fuselage barrels especially from a manufacturing point of view. Special interest was dedicated to the machines and clamping systems required for the application and the related process and lead times. The information provided in this thesis is centred around the introduction of the recent FSW process in the production of fuselage barrels, and does not pretend to show the detailed technical implementation of this technology in future metallic fuselages.
9.1 Airbus an EADS Company

Airbus started in 1970 as a consortium of European aircraft manufacturers based in France, Germany, Spain and the United Kingdom, which was created to compete against major aircraft manufacturers from the USA. Thirty years later, Airbus formally became a single integrated company. The European Aeronautic Defence and Space Company N.V. (EADS) and BAE Systems transferred the corresponding assets to Airbus and became shareholders. Currently, EADS is the only shareholder, having bought the BAE Systems shares in 2006. For this reason the company was renamed to Airbus an EADS Company [267].

Airbus consistently captures around half of all orders for airliners with more than 100 seats worldwide [267]. More than 300 operators around the world operate almost 6500 Airbus aircraft, and support is provided for these in-service aircraft. The company competes very well against other aircraft manufacturers, having received a very high number of orders. While the total number of deliveries is over 6700 aircraft, the total backlog orders are around 10700 aircraft of different types [267].

The international layout of the company may be verified by the fact that Airbus, with headquarters based in Toulouse, France, employs around 52500 people of over 80 nationalities directly in several countries. Additionally, a wide range of suppliers around the world works with the company. Therefore the official working language of the company is English, but the ability to understand a great diversity of languages and cultures has helped it to reach customers all around the world. The plant in Hamburg, Germany, is the main design and production site of the Airbus Operations GmbH and employs around 12000 people [267].

Airbus is a leading player in the field of civil aviation. Its product portfolio for this market currently ranges from the 107 to 117 seat Airbus A318 to the 555 to 853 seat Airbus A380.

The short and middle-range A320 family of aircraft was introduced in 1984 with a very modern design, including fly-by-wire controls and side-sticks, and is now one of the best-selling aircraft of Airbus [267]. Its popularity leads to the need for developing future versions of this aircraft, including the A320neo (New Engine Option) to be delivered in the next years. Since the development process of such complex aircraft requires a high effort, the R&T department of Airbus is already concerned with a short to middle-range aircraft which is able to replace the A320 family in the next decade. New technologies have to be investigated, and among others the best balance between cost and performance has to be defined. In this context, FSW is being investigated for different applications in an aluminium version of this aircraft.
9.2 Friction Stir Welding at Major Component Assembly

The goal of the work presented hereafter was the study of the applicability of FSW at MCA level for the typical fuselage sections. These are the parts of the fuselage that are approximately cylindrical, excluding therefore the tail and nose sections. Only the joints performed at MCA level were inside the perimeter of this work.

9.2.1 Comparison with composite structures

For future fuselage structures of aircraft for the civil aviation market, traditional aluminium structures compete directly with composite structures. While composites may provide advantages when looking at the ratio between stiffness and weight, their higher cost and lower small-impact resistance still favour metallic structures.

The decision for building a composite or metallic fuselage is not only driven by technical decisions, but also by the companies’ strategy and market expectations. It is therefore a safe approach to develop future fuselage concepts in parallel using both materials. Besides the obvious advantage of being able to deliver a fuselage in either material in short time, the internal competition enhances the final design, be it composite or metallic. Furthermore, this parallel approach allows deciding for a mixed approach including both material types. Due to the higher cost involved, such a dual approach does only make sense in early development stages. Anyway, the technology developed in this stage can be applied on different aircraft in the future and is not limited to a specific project. Investigations based on the requirement to compete against lightweight and reduced part count composite structures, have led to a high interest in introducing welded joints in aluminium structures at different manufacturing stages.

9.2.2 Comparison with riveted assembly strategies

Advantages of FSW when compared to riveting technologies for assembly of major components include the reduced part count, reduced assembly time and better suitability for automation. FSW offers a very high reliability and is generally described as leading to lower distortion and residual stress when compared to fusion welding processes. The low defect susceptibility and high repeatability of the process make it an ideal choice for replacing mechanical fasteners.

A rivet-less assembly strategy would lead to several advantages, among them, a simplified inspectability, especially if riveted lap joints can be replaced by FSW butt joints. In this case inspection would only be required from one side of the joint, reducing both,
the required over-dimensioning and also inspection costs. Furthermore a welded joint does not have the same susceptibility for Multiple Site Damage (MSD) and therefore the long term maintenance costs may become reduced.

9.2.3 Maturity of the welding technology

The maturity of FSW for application in an aircraft fuselage barrel strongly depends on its specific application. At shell level, using well known materials such as the aluminium alloy AA2024, significant knowledge is available. At component level, this joining technology is already successfully applied by Airbus and other major companies in the aeronautical sector [7, 37, 63, 268–271]. This is however not true for the application of FSW for assembly of larger components and more recently developed alloys. Different FSW variants have to be studied in this context. While in some cases standard FSW tools may be the most adequate solution, in other areas a bobbin-tool solution would be preferable. Therefore, the applicability of this joining technique has to be studied in detail for each application, demonstrating possible challenges and its advantages.

9.2.4 Joint configuration

Overlap welds are technically feasible under certain conditions, but this joining technology shows most advantages when applied in a butt joint configuration [272]. Fatigue initiation at the overlap is prevented by design in this case. In some cases, for smaller aircraft for example, overlap welds may be feasible because of their lower stress level. Due to the mechanical performance advantage of butt joints in relation to overlap joints and the impossibility of completely protecting the free surface on overlap joints at the interface, butt joints are currently considered the only viable way of joining large fuselage barrels.

The joint is best located in an undisturbed area, since long rectilinear welds are the most efficient way to join using FSW. On the other side, the robotic weld may be performed upside-down and other positions not recommended for riveting by operators. Therefore also the joint locations on the fuselage barrels may be optimised for the new assembly strategy.

One further aspect that has to be considered for a FSW joint is that both, the initial part of the weld and the end should be made in sacrificial material, since mechanical properties are lower in these sections. For this reason, the sheet material for FSW has to be designed larger than for traditional riveted structures. This may increase the
cost, either due to the larger material size, or due to longer manufacturing times, if the sacrificial material is added by welding for example.

9.2.5 Repairability of the joints and welded structures

One challenge for FSW at assembly level could be in-service repairability. Traditionally, for big defects, whole panels may be replaced on a fuselage. In the case of a welded joint, these panels may not be easily replaced, since it is not currently feasible to perform in situ welds on a completed aircraft structure for repair purposes. It is however expectable, that the advantages at manufacturing level outweigh these possible challenges. Local patch repairs may still be performed in the same way as on traditional riveted fuselage barrels, which eases the introduction of this new joining technology. This has to be considered in the design phase of the panels though, since enough space and thickness has to be foreseen to also allow rivet installation near the prior location of a FSW seam. Furthermore, the application of FSW for repair applications is being examined by different entities [273].

In terms of repairability, it should also be mentioned that FSW joints may be re-welded keeping the expected mechanical properties. This is important, since, even during manufacturing, problems like a broken tool could arise. Such issues could simply be corrected by re-welding the corresponding joint.

9.2.6 Tolerance study

While an overlap configuration would allow for relatively high tolerances, a butt joint requires much higher accuracy, since only very small gaps may exist between two shells to be welded, see Section 5.3. The tolerances have to be almost ten times as tight for a sound butt joint configuration, as for overlap joints. Therefore the tolerance management for an assembly strategy based on FSW is an important aspect to be studied in detail.

For example when longitudinal joints are performed on a barrel, the final diameter and shape have to be correct. As opposed to riveting, using FSW for longitudinal joints, it is not possible to keep a part of the joint open for easier circumferential joining of two barrels. The geometry after welding has to be the final geometry, without the need for further corrections except minor deburring for example, as it is currently possible using overlap riveting. Therefore special jigs and tools have to be developed.

The tolerance study is complex due to the large number of involved components, manufacturing steps and technologies, which all influence the final geometry of the fuselage. This also demonstrates the reason for studying this topic in detail, since an optimisation
of the whole tolerance chain is required for guaranteeing the final shape of the fuselage, while keeping the possibility to perform butt joints, where geometrical corrections would be very difficult to apply.

9.2.7 Time and cost

In order to understand the feasibility of FSW at MCA not only technically, but also in economical terms, the team was involved in the definition of all process steps required for performing such joints. The associated process times have shown that FSW may lead to significant improvements when compared to a riveted approach [274].

It has to be well understood how the introduction of FSW at MCA level influences the process and lead times for manufacturing. It is expected that these times may be significantly reduced. While the welding speed is vastly superior to the riveting speed for a certain joint length, due to the relatively high preparation time for welding and the even longer surface protection time required after welding, the applicability of FSW to very short joints is questionable. By designing adequate clamping devices, the preparation time may be minimised. This is a topic of high importance for the introduction of this joining technology at assembly level. The overall timesaving has to be demonstrated, and due to the high cost concerning new tooling, the reduction in lead and process times has to be significant.

Concerning cost reduction, the number of operators may be reduced due to the high level of automation possible with FSW. While this not only reduces costs at long term, it also allows guaranteeing a constant quality, even with very high work loads. This would be a further step into a “factory of the future” [275]. FSW does not require special welding skills comparable to traditional fusion welding since it is a mechanised process.

The production chain for metallic fuselages assembled by riveting has been optimised for over 40 years. It is therefore a very hard task to provide a similarly optimised solution for the FSW based assembly process in a short time period. Currently the final conservation and surface protection of the barrels is done after all joints and connections are made for example. The introduction of FSW requires surface protection of the joint directly after the welding process. The access below the stringer and frame couplings may not be good enough for surface protection at a later stage. For this reason the manufacturing chain, optimised for riveted assembly, has to be adapted for the new assembly strategy.

New tools and jigs have to be developed for being able to join large structures in a butt joint configuration with high accuracy. This has to be kept in mind when calculating the total cost of introducing FSW at assembly level.
9.2.8 Surface protection

A further challenge to be intensively studied for this application of FSW is corrosion protection. It is not feasible to anodise a complete fuselage after assembly. Therefore local protection strategies have to be developed for the joint region, which are comparable in performance to the traditional approach. It should in this context be remembered, that a fuselage barrel for application in civil aviation has a life expectancy which lies beyond the expected life of most ground vehicles. Additionally, contrary to some applications of FSW in the rolling stock or ship building industries, no corrosion is acceptable during the whole product life, since the highly stressed fuselage barrel is subjected to fatigue loads, and corrosion could lead to crack initiation.

Surface protection is an important aspect to be considered for lead and process time estimations. The drying time of the different paint layers may exceed the welding time by several times for example. Surface protection is often required directly after welding and may therefore not be performed after all joints are complete, as is currently done with riveting. This is especially true in regions where access is difficult or impossible after the joint is complete, for example below stringer or frame couplings.

The surface protection time may also be optimised. A significant part of the total time in surface protection applications is the drying time of the different layers. This drying time may for example be reduced using thermal radiation devices. Furthermore the coatings may be left drying during the night shift while no other tasks have to be performed. This requires a detailed planning and optimisation of the manufacturing steps. Due to the high risk of explosion of the fumes created during evaporation of the solvents used in most coatings, it is not recommended to perform works where sparks could be created during flash off time.

9.3 Conclusions

It can be concluded that FSW has reached a significant technology readiness level for application in fuselage structures, even including at MCA level. Research is however still required before being able to shift the technology over to a specific application for assembly of large components at a competitive cost.

Especially the required tooling and the surface protection scheme after joining have to be thoroughly studied and optimised in order to achieve a lean manufacturing chain. It is expectable that MCA using FSW will have big advantages in terms of time and cost in relation to traditional assembly strategies after some optimisation time.
Chapter 10

Conclusions

The present chapter seeks to provide the main conclusions drawn in the different parts of this work, further relating the different chapters with each other. While each chapter contains its own conclusions, here the most important aspects are reinforced.

After summing up the main conclusions, a short outlook on future evolutions and work in progress is provided.
10.1 Conclusions and general remarks

While researching the state of the art related to the proposed work, it was found that a serious lack of information existed, among others, in the following areas. Clamping systems for FSW, thin plate distortion due to rolling residual stresses, accurate residual stress measurement of thin walled structures including through-the-thickness information, fatigue behaviour of thin walled structures subjected to a biaxial strain field and instrumentation using FBG based sensors. In all topics a significant advance could be achieved during thesis preparation.

A detailed material characterisation of all aluminium alloys used in the presented work was performed and represents a solid base for result interpretation.

FBG sensing devices have been developed for application of monitoring, proving their applicability to such scenarios, including FSW. Since the calibration constants of these sensors partially depends on the bonding approach used, practicable calibration procedures were developed for both temperature and strain measurement using these devices.

In the area of FSW the lack of information regarding the influence of the clamping force on residual stress and distortion was significantly reduced. It was successfully shown that with higher clamping forces, lower distortions can be found, while only very slightly affecting residual stresses. The difference in the longitudinal residual stress between lower and higher clamping forces is limited to its though the thickness distribution which was successfully determined by the contour method.

It was shown that the plate distortion is due to changes in a residual stress field with a very low magnitude. The large area affected by the stresses and its through the thickness variation strongly influence the shape distortion.

Several residual stress measurement techniques were used. It was also shown that diffraction based techniques often do not provide the required detail. In some advanced alloys and especially in the welding regions, problems also arise due to the grain size and its variation. On the other hand, relaxation based methods have been able to deliver the required results, although at the price of being completely invasive.

For very detailed through the thickness residual stress measurements required for predicting the distortion of complex small thickness shells produced by HSM, the layer removal technique was adapted and developed. The proposed approach is applicable in well equipped workshops, since it only requires a precise high speed milling machine and corresponding CMM. While no exact prediction of the plate distortion after machining could be obtained, both the tendency and the magnitude of the prediction were correct.
This is a useful step forward for milling workshops concerned with tight tolerances as it is the case in the aeronautical industry.

Fatigue specimens produced by FSW and HSM were successfully tested in a biaxial stress field, and a strong influence of the rolling direction on the crack growth path was found.

Knowing the SIF allows to predict the crack growth behaviour. For complex structures, for example under multi-axial loading and including residual stresses, the calculation of the SIF is very difficult and mostly not feasible. For these cases an experimental approach based on the measurement of the actual strain field around the crack tip was further developed. The proposed approach is another step forward in this measurement technique applicable to thin cracked plates.

Several equipments, parts and procedures were developed during this work. These are shown in detail in the appendixes of this thesis in order to serve as a basis for future evolutions.

This thesis allowed to study the limits of applicability of several measurement devices and techniques, including surface strain and residual stress measurement methods.

The internship performed in the aeronautical industry has shown that the studied concepts are of high importance for manufacturing aircrafts. The achieved knowledge may contribute positively to the evolution in this industry.

\section{Work in progress and future evolutions}

This thesis includes several topics of high interest, and naturally, each of them may be further investigated. A good starting point for a in-depth analysis with a high number of specimens for some of the works was provided. For example the layer removal technique procedure could be optimised with some more experiments since very promising results were obtained with only little optimisation.

Some topics that could be investigated in the future include long-term installations and fatigue phenomena of FBG sensors. A further evolution of FBG based sensors for measuring temperature and strain during welding should be distributed sensing, since this could potentially reduce or even eliminate problems related to the high gradients found in welding situations, and more information could be gathered by these sensors, leading to a better knowledge of the studied welding processes.
The iHDT technique is widely applied for engineering residual stress measurements. Based on the knowledge regarding this technique and the FBG sensing devices developed, FBG based iHDT rosettes have been developed and are currently under investigation. One of the main advantages of this approach is the elimination of any influence due to electromagnetic interferences in the measurements. Also, due to the high strain sensibility of these sensors, a higher accuracy is expected.

Topics of interest certainly include a more complete study of fatigue under biaxial loading, including base material specimens, initial cracks in different locations in relation to the joint and different load ratios and loading scenarios.

More experiments and a further optimisation of the SIF determination algorithm can certainly lead to an interesting application for real-world applications, allowing to predict the crack growth behaviour of big thin-walled structures by a simple measurement.

The clamping systems used for FSW should be further studied, since a better understanding of the best clamping location and force certainly will help in the proliferation of this joining technique in many different industries. Therefore more experiments are suggested concerning this topic.

Summarising it may be said that the high number of investigated topics provided a significant knowledge increase, while at the same time creating interesting opportunities for further investigations.
Appendix A

Nomenclature used for pictures and specimens

The Figures and Specimens for all works have been named following the convention explained below, which was adapted form the HZG convention. This approach has the advantage of being able to keep track of files used in different publications more easily, allowing a better verification of published results. The additional effort required to use such a naming scheme is negligible.

A.1 Naming convention

A.1.1 Figures

T-PPP-AAA-DDDDDDD

T  type of figure (P - picture; G - graphics and diagrams; T - Table; D - Drawing)

PPP short form of the project name (COI - coins project Gesellschaft für Kernenergieverwertung in Schiffbau und Schifffahrt (GKSS); WEL - welding project FEUP; PhD - other specimens made for PhD; IM or IMG - GKSS IMAGINE project)

AAA  author (VRT - Valentin Richter-Trummer)

DDDDDDD  identification (for type G: T - tensile; C - charpy; MFT - micro flat tensile; HV - hardness; BD - bend test; TC - temperature measurement; WD - weld diagram; FC - force; S - strain; NH - Nanohardness; MH - Microhardness; CG - Crack growth; SN - crack initiation)
for the type P: Ma - Macrograph; Mi - Microstructure; EQ - Equipment; Ch - Chart; Pr - procedure; Sp - Specimen)

For example the Figure P-COI-VRT-Pr-TWB_01 shows a Picture related to project COst effective INtegral metallic Structure (COINS), taken by Valentin Richter-Trummer showing the test procedure. It’s description (TWB) says that it shows tailor welded blanks and it is the first picture taken.

A.1.2 Data files

T-PPP-AAA-DDDDDD

T type of file (DA - data file)

PPP short form of the project name (COI - coins project GKSS; WEL - welding project FEUP; PhD - other specimens made for PhD; IM or IMG - GKSS IMAGINE project)

AAA author (VRT - Valentin Richter-Trummer)

DDDDDD identification

For example the Figure DA-COI-VRT-ARAMIS_TW26 is a data file relates to the GOM mbH Aramis measurement of TWB26 plate in the COINS project, produced by Valentin.

A.1.3 Specimens

PPP-AAA-TTT-NNN;

All information regarding the specimens used in this work is stored in a central file.

PPP short form of the project name (COI - coins project GKSS; WEL - welding project FEUP; PhD - other specimens made for PhD; IM or IMG - GKSS IMAGINE project)

AAA author or responsible; contact this person for further information (VRT - Valentin Richter-Trummer)
Appendix A. *Nomenclature used for pictures and specimens*

**TTT** Specimen type (welded plates don’t need this, only NNN; B3 - 3 Point Bending; B4 - 4 Point Bending; T - tensile; CT - Compact tension; MT - Middle tension, center crack tension; Ma - Macrograph; Mi - Micrograph; MH - Microhardness; SN - fatigue initiation)

**NNN** specimen description and numbering

For example the specimen COI-VRT-DUMMY_ARAMIS_1 comes from project COINS, being the responsible contactable for further information Valentin Richter-Trummer. The specimen description itself means that it is a dummy specimen for the Aramis measuring system, and it was the first specimen tested.
Appendix B

The contour method for residual stress measurement

Compact tension specimens are widely used for fatigue crack growth characterisation. It is well known that when using this type of specimen for welded samples, residual stresses affect the crack growth behaviour, in particular reducing the crack growth rate when the initial notch is parallel to the joint. Several approaches have been used to characterise the residual stress field in welded compact tension specimens, including traditional destructive relaxation based methods, as well as non-destructive diffraction based techniques. The problem with the approaches presented so far is that the former do not provide through the thickness results, whereas the latter, although capable of providing through the thickness measurements, are only available in very few facilities worldwide. The interest in thick welded samples, where substantial through the thickness variations of residual stress fields are to be expected, leads to the need for a full field characterisation. In the present work, the residual stress perpendicular to the crack growth path in two welded steel compact tension specimens was measured by the contour method, an emerging destructive method. A full surface map of the residual stresses was obtained. This full field information on the residual stress distribution reveals substantial through the thickness variations. High compressive stresses were found in the centre of both specimens. The compact tension specimen with the crack plane in the centre of the weld bead presented slightly higher compressive residual stresses. The strong variation of the stress field, both in the longitudinal as well as in the through the thickness directions, emphasises the need for full field residual stress measurements for the correct interpretation of fatigue crack growth tests when using this type of specimen.
B.0.4 Specimen details

Compact Tension (C(T)) specimens with a thickness of 32mm and according to ASTM E647 [177] were extracted from steel plates welded under similar conditions to those used for the new railway bridge at Alcácer do Sal in Portugal. The aim of this study is to measure the through the thickness residual stress distribution in these specimens, in the wider context of a programme of fatigue behaviour characterisation. The present appendix concentrates on the first of these aspects.

Figure B.1 shows the geometry of the specimen considered, where the thickness was chosen to be similar to the thickness of the structural details of interest in the before mentioned bridge.

![Figure B.1: Geometry of the C(T) specimen.](image)

Figure B.2 shows schematically the location chosen in the welded plate for the extraction of the specimens, taking into consideration the relative location of the specimens plane of symmetry and of the weld bead profile.

B.0.5 Material properties

The material used in the analysed construction is the S355NL normalised rolled fine-grain structural steel according to the European Standard (EN) standard 10025 [276].

For the linear elastic FEM calculations, the Young’s modulus and Poisson’s ratio of 210GPa and 0.3 respectively were used. The yield strength was determined in laboratory and is near 400MPa [277]. Welding of these specimens was performed in 36 passes using the Metal Active Gas (MAG) welding process with a double-V preparation of the joint surfaces.
In specimen CT2, the weld bead was in the centre of the specimen, while in specimen CT1 a offset of approximately 9mm exists from the specimen centre to the welding line centre. This was done in order to study the crack growth behaviour of the weld material zone (specimens CT2) and approximately the HAZ (specimens CT1). Because of the double V joint preparation, the latter is not, strictly speaking, an heat affected zone specimen, but instead it samples three regions, namely, HAZ, base material and even welded material.

B.1 Residual stress measurement

This work presents an application of the contour technique in this context, showing the complete residual stress field for crack planes (plane of symmetry of the C(T) specimen) located in two different positions relative to the weld bead profile. This appendix provides therefore more detailed information on the residual stress fields of this very commonly used specimen geometry than hitherto available. One of the inherent advantages of the contour method in the context of welding residual stress measurements, is that the measurements do not depend on the difficult to obtain free lattice parameter $d_0$ as is the case with diffraction based techniques [278].

As explained before, within the wide range of residual stress measuring techniques, the contour method was deemed to be the most appropriate, due to its ability to deliver a full 2D contour map of the residual stresses inside of a specimen in the direction of interest. Price et al. [279] mentioned the importance of considering the gauge volume in residual stress measurements. While traditional strain gauge based measurement techniques provide gauge volumes in the order of over 10mm, neutron diffraction allow to reduce this volume to 0.5mm in certain cases [279]. In the contour method, the gauge
volume is related to the displacement measurement grid, the finite element mesh size and the filter applied to the measured displacement. In the present case, the element size was below 0.5mm, and since the contour method calculates the stress tensor in accordance with the finite element mesh, this method's resolution is estimated to be below 1mm [280]. It should however be noted that the gauge volume of the contour method in the present case is larger than this 1mm due to the applied filter, leading to smoother results. The fact that the method is fully destructive did not influence the decision in the present case, because of adequate availability of samples.

The contour method is a destructive experimental technique able to determine a full surface map of one component of a residual stress field based on four steps [85, 103]. First the specimen of interest is cut using wire electro discharge machining, revealing the surface of interest. In the second step, the surface topography is measured on both plate halves accurately, and in the third step these two measurements are averaged and data are conditioned for applying to a finite element model. The fourth step consists in the solution of a FEM model representing the specimen. The conditioned displacement data are applied to the model and the stresses on the surface of interest are retrieved. Several studies have been performed to verify the contour method by neutron diffraction and other well established techniques, proving its validity, e.g. [278, 280–282].

B.1.1 Cutting of the specimen

The specimens were cut using a Sodick wEDM equipment with a 0.25mm diameter wire at a speed of approximately 2.4mm/min. The specimens were clamped on both sides of the cutting line as can be seen in Figure B.3. An higher guarantee of elimination of plasticity effects near the cut, could be obtained by clamping the specimen rigidly closer to the cutting line. Nevertheless a low error due to plasticity effects is expected in the present case [104].

Both specimens were cut in the same way using the same cutting parameters and clamping setup.
B.1.2 Measurement of the cut surface

Measurement of the cutting surfaces was performed at CATIM, Porto. A Zeiss UMPC Ultra touch probe based CMM was used, guaranteeing a high degree of precision. A 1 mm diameter ruby tip was used for the slow measurements. The roughness created during an wEDM cut is low enough for proper displacement measurements using this touch probe. Optical displacement measurement methods may be more affected by this cutting procedure, although good results have been reported before [103, 278]. The whole cutting area was measured up to a distance of 0.5 mm from all four borders. 3360 points were acquired with high accuracy. The measured points have a distance of 1 mm in the width and thickness directions, which is enough for the representation of the actual shape in the present case. Figure B.4 shows the representative raw measured surface relaxation of the plate half B on specimen CT2. The other measured surfaces look very similar and are therefore not shown.

Figure B.3: Cutting setup used in the wEDM machine.

Figure B.4: Relaxed surface after the cut; Raw measurement data.
B.1.3 Data conditioning

Data conditioning was performed in MatLab [245]. The first step of the data conditioning procedure consists in selecting the same origin for both specimen halves in order to be able to map the measured points to the same data grid.

The second step in the data conditioning approach selected for the present problem is to align the best fit plane through the centroid of all measured points to a horizontal plane though the origin. This will help to guarantee the necessary force and moment equilibrium of the residual stresses on the plane of interest. In this way, the necessary stress equilibrium on the surface is enforced.

In the next step the measured point cloud is mapped to a rectangular grid, and the average of the grid points is taken. Figure B.5 shows the average of both relaxed surfaces which were measured by CMM for the specimen CT1 and the corresponding smooth surface. This smooth surface was calculated by fitting the measured average point cloud to a bi-variate spline [283].

![Figure B.5: Relaxed surface after the cut for specimen CT1.](image)

Figure B.6 shows the average of both relaxed surfaces which were measured by CMM for the specimen CT2.
Surface smoothing was based on the bi-variate spline routine provided by the MatLab spline toolbox. Quadratic splines were chosen, and only 3 knots were defined in specimen width direction, and 5 in its thickness direction. This low number of knots was chosen, since the measured relaxation range was near the measurement accuracy of the available equipment, and therefore no more detail should be extracted from the noisy measured data. For the specimen CT1, the Goodness of Fit (GoF) defined by the average difference between the average and the smoothed surfaces is 98%, for the specimen CT2 the GoF is 99%, reinforcing that the chosen fitting surface accurately represents the obtained data.

B.1.4 Stress calculation by the FEM

A linear elastic model of one half of the C(T) specimen was made using the Abaqus v6.9 FEM package [284]. Figure B.7 shows the used FEM mesh refined near the stress calculation site only. 886336 linear brick elements were used with reduced integration (Abaqus reference: C3D8R).

The only constraints applied additionally to the measured deformation were one node in two perpendicular directions and a second node in one direction in order to prevent rigid body motion and rotation. The displacement was applied to the whole cutting surface. It was extrapolated to the not-measured pre-crack region in order to eliminate stress singularities in the transition zone. After the stresses were calculated, this extrapolated region was again eliminated from the final results. This extrapolation changes the measured stress profiles slightly. The equilibrium of all stresses is calculated taking into account only the realistic stress values and not the almost infinite values of the transition zone that would be obtained without extrapolation. Only stresses perpendicular to the
Figure B.7: FEM mesh used for calculating the residual stresses based on its relaxation.

cutting surface are retrieved on the nodes of interest. The calculation for each model on a dedicated quad core Intel Xeon workstation running Linux took approximately 4h.

B.2 Discussion of the results

Figure B.8 shows the calculated residual stress fields perpendicular to the surfaces of interest.

Figure B.9 shows the calculated residual stress field perpendicular to the cut surface mapped onto the C(T) specimen geometry with the represented weld beads.

As it can be seen, significant compressive residual stresses were found in the centre of the specimen, which is in accordance with the common knowledge of residual stresses in multi-pass fusion welds. Figure B.10 shows the residual stresses measured along two lines through the specimen, one near the surface and the other in the centre. In this context it should be mentioned that the stresses calculated very near the surface (less than 0.1mm from the surface) should not receive too much attention. The displacement applied in the FEM model near the surface are always extrapolated in the contour method, since it is not possible to precisely measure the surface deformation very near the body boundaries.

While the obtained residual stress profile seems to be similar in both cases at first sight, it is noticeable that the measurement at the HAZ section shows a wider compressive area.
Figure B.8: Calculated residual stress perpendicular to the cut surfaces. For detailed information on this contour maps location on the specimen, see Figure B.9. The artificial initial notch is located at 105.4 mm in these graphics.

Figure B.9: Calculated residual stress perpendicular to the cut surface mapped onto the C(T) specimen geometry. The specimen back-face shows the approximate weld bead profile.
in the measurement plane centre, and a narrower tensile zone near the specimen borders. Furthermore, in this plane, the surface is not loaded with as high tensile residual stresses as is the case for the measurement plane through the centre of the welded joint. With these two measurements it is therefore possible to see part of the tendency of the residual stress evolution as a function of the distance to the plane through the welded joint.

Possible errors in the contour residual stress measurement technique are expected to be small, since already validated procedures were followed though the whole measurement process. Both the techniques inventor [103, 281] and the present author [236] have shown the ability of the contour technique to accurately measure residual stresses. In order to further reduce plasticity related error sources during the cut, a more advanced clamping system could be designed, which allows to clamp the specimen very near the cutting line.

Although their measurements were performed on quenched and not welded specimens, Lados and Apelian have shown compressive residual stresses near the notch and a similar residual stress distribution away from the notch as obtained in the present work [285]. Those authors used the cut compliance technique, and therefore Lados and Apelian [285] do not provide complete through the thickness information.

Some literature data exist on the residual stress distributions in compact tension specimens with a weld seam parallel to the initial notch, which allow a qualitative comparison of the present results. Several works cite similar residual stress distributions [86–90], although none shows the complete residual stress map determined in the present work. Beghini and Bertini [86] use the sectioning technique with strain gauges, providing surface relaxation based data only. Kitsunai, [88, 89], also uses the sectioning technique
with strain gauges. Later on, the same author used XRD [90] providing surface residual stress measurements only. Ghidini and Dalle Donne [87] use the cut compliance technique with back face strain gauge obtaining an average effect of the through the thickness residual stress distribution.

An attempt at the measurement of through the thickness data was presented in 2005 by Rading [91], showing the residual stress distribution in smaller and thinner welded aluminium C(T) specimens. neutron diffraction was used, and data for two situations was acquired: near surface and mid thickness, although the technique used by Rading could in principle provide full field data. This limitation is most likely related to the beam time necessary for the acquisition of each data point. The shape of the residual stress distribution along the distance from the notch is almost identical to the present case, but the magnitude differs which is not surprising, given the different material and C(T) specimen dimensions studied. Even so, the tendency is the same for both cases, being the mid-thickness residual stresses lower than the surface residual stresses, which is expected and shown to be more pronounced in thicker welds.

The present work extends published information on the residual stress distribution in C(T) specimens of welded steel contributing new full-field residual stress data for the case of weldments parallel to the crack plane.

As discussed before, full-field measurement of residual stress can be obtained using for example neutron or synchrotron x-ray diffraction techniques. These techniques are available in just a small number of locations worldwide. The widespread interest on fatigue crack growth characterisation tests using C(T) specimens, and the importance of residual stresses in those tests, highlight the interest of an alternative procedure for residual stress measurement. The present work uses the contour method to achieve that goal, using commonly available equipment.

B.3 Conclusions

Full field residual stress distributions in the crack plane of thick welded steel C(T) specimens were determined using the contour method. The residual stress distribution is roughly similar in shape to a saddle, and presents compressive residual stresses at the notch, with considerably greater absolute value in the mid thickness region than at the specimen’s side surfaces. Near the specimen surface, the residual stresses perpendicular to the notch are slightly tensile. As expected for a multi-pass fusion weld, compressive
residual stresses perpendicular to the welding line were found in the centre of the specimen. It is shown that for welded steel C(T) specimens, with a thickness of the order of 30mm, a substantial through the thickness variation of residual stresses exists.
Appendix C

Source code of the SIF determination algorithm by DIC analysis

Matlab was used for the implementation of this algorithm. The code is run by calling the Exp_K.m function. This file will then call all necessary nested subroutines.

The Exp_K.m requires some user input, among them the file of the strain measurements to be used. This function then calls overd_stressv7.m for stress calculation around the crack tip. The crack tip location is calculated inside the stress calculation function, and corrected by the results obtained in Exp_K.m. After having determined the stress around the crack tip, the function overdCII_nt.m runs the overdeterministic algorithm and responds with the stress intensity factors.

C.0.1 Input file

The input file necessary for this algorithm includes some information regarding the location of the different files which contain strain measurement data and also additional information regarding the crack tip location which may or may not be used based on the decision of the user.

An example input file is given below. It should be noted that the algorithm uses the line number as a reference, and therefore no line may be deleted or added by the user without modifying the algorithm itself. In lines 2 and 3 the locations of the ASCII files with strains in X and Y directions are given. Line 5 provides the location of an output file which may be used for some tests. Lines 7 and 9 provide the mechanical properties
needed for calculation of stress based on the strain information. If the manually detected crack tip is to be used, this value of its location on the image is read from 13, and in line 11 the crack tip location is referenced to the centre of the specimen as additional information which is not used in the algorithm. Details concerning the parameters used for strain measurement are given in line 15 for reference only. Line 17 and 19 define the resolution and location of the strains used for SIF determination. The last line provides information of the macroscopic crack growth direction.

<table>
<thead>
<tr>
<th>Line</th>
<th>Description</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td><code>% name of the input files containing X and Y strains respectively</code></td>
</tr>
<tr>
<td>2</td>
<td>ARAMIS/complete/MT80_SIF_0000_a1_14/results/DA-PhD-VRT-0000_a1_14_ex</td>
</tr>
<tr>
<td>3</td>
<td>ARAMIS/complete/MT80_SIF_0000_a1_14/results/DA-PhD-VRT-0000_a1_14_ey</td>
</tr>
<tr>
<td>4</td>
<td><code>% name of the output file</code></td>
</tr>
<tr>
<td>5</td>
<td>result_files/MT80_SIF_0000_a1_14_overdeterministic_stressfield.txt</td>
</tr>
<tr>
<td>6</td>
<td><code>% Youngs modulus</code></td>
</tr>
<tr>
<td>7</td>
<td>69.64E3</td>
</tr>
<tr>
<td>8</td>
<td><code>% Poisson coefficient</code></td>
</tr>
<tr>
<td>9</td>
<td>0.33</td>
</tr>
<tr>
<td>10</td>
<td><code>% Real coordinates of the crack tip measured on the specimen (X and Y)</code></td>
</tr>
<tr>
<td>11</td>
<td>-14.02 0</td>
</tr>
<tr>
<td>12</td>
<td><code>% Coordinates in ARAMIS of the crack tip measured on the specimen picture (X and Y)</code></td>
</tr>
<tr>
<td>13</td>
<td>3.709 -0.045</td>
</tr>
<tr>
<td>14</td>
<td><code>% ARAMIS calculation parameters (facet size, facet distance, strain size)</code></td>
</tr>
<tr>
<td>15</td>
<td>8 6 25</td>
</tr>
<tr>
<td>16</td>
<td><code>% Interpolation step for the grid</code></td>
</tr>
<tr>
<td>17</td>
<td>0.05</td>
</tr>
<tr>
<td>18</td>
<td><code>% X coordinates for the overdeterministic matrix (distance after crack tip)</code></td>
</tr>
<tr>
<td>19</td>
<td>0.1 7.5</td>
</tr>
<tr>
<td>20</td>
<td><code>% side of the crack ('left' or 'right')</code></td>
</tr>
<tr>
<td>21</td>
<td>left</td>
</tr>
</tbody>
</table>

### C.0.2 Exp_K.m

Input variables may optionally be set, otherwise the code will ask for any information that is necessary and has not been given. Further input variables are given in the input file:

- **file** Full filename of the file with the necessary input data
- **mode** Mode I or mode II can be chosen with this variable
- **plot_figures** If this variable is set to 1, verification plots are made, otherwise no plots are made in order to speed up the algorithm
- **actd** If set to 1, the crack tip is automatically detected, otherwise the manually defined crack tip from the input file is used
- **order_of_terms** Vector with the order of terms used in the series expansions
Output variables:

**order_of_terms** Vector with the order of terms used in the series expansions

**test_I** Vector with the mode I SIF as a function of the order of terms

**test_II** Vector with the mode II SIF as a function of the order of terms

Below is the commented source code for Matlab:

```matlab
function [order_of_terms, test_I, test_II] = Exp_K(file, mode, plot_figures, actd, ctc, order_of_terms)

% This function joins the Overdeterministic SIF calculation algorithm and
% the ARAMIS stress calculation and preparation algorithm
% Valentin, Pedro, 04.08.09

% %%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%
% if no input parameters are given in when calling the function, the
% following data are used
tic
if nargin==0
    close all
    mode=2; % 0: for verification purposes with a theoretical result
    % 1: mode I SIF only
    % 2: mode I and II SIF
    plot_figures=0; % if set to "1", plots will be made;
    % if set to "0" no plots will be printed
    actd=1; % if set to "1", Automatic Crack Tip Detection will be used,
    % if set to "0" the manually detected crack tip from the file
    % will be used
    actd_series=0; % if set to zero, the automatically detected crack tip
    % will not be corrected until the series converges

    % number of terms used for the series expansion
    order_of_terms=[12 13 15 17 18];
    order_of_terms=3:20;
    ctc=[-0.4 0]; % correction of the automatically detected crack tip
    % (should be chosen in a way that guarantees the series
    % convergence)
end

% %%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%
clc
if actd_series==0
    ctc=[0.35 -0.1];
end
```
if mode==1 || mode==2
  % reads the input file with details about the problem
  if nargin==0
    [filename, pathname] = uigetfile('*.txt', ...
      'Please select the input file');
    if isempty(filename)
      stop, clc
    end
    file = fullfile(pathname, filename);
  end
end

% this loop is used to correct the assumed crack tip location based on the 
% knowledge that the series expansion has to stabilize. Therefore, some 
% iterations are run with different corrections in order to find the 
% stabilized solution (use order_of_terms=[12 15 18];)

disp(file)

if actd_series==1
  ctc_inc=0.4;
  contador_max=round(abs(2*ctc(1))/ctc_inc+2);
elseif actd_series==0
  contador_max=1;
end
for contador=1:contador_max

  % call the ARANIS stress preparation function
  disp('  ')
  disp('  ')
  disp('Calculating the stress field around the crack tip')

  if mode==1 || mode==2
    disp(strcat('Crack tip location correction is: ', ... 
      num2str(ctc(1)), 'mm'))
    matrix=overd_stressv7(file, actd, ctc, plot_figures);
  end
  if mode==0
    matrix=overd_stressv5_verif(actd, plot_figures);
  end

  % call the overdeterministic SIF calculation function for mode 1 or 2
  disp('Calculating the stress intensity factor')
  disp('  ')
  f=0;
for n=order_of_terms
    f=f+1;
    if mode==1 || mode==0
        K1=overdc(matrix(1,:),matrix(2,:),matrix(3,:),matrix(4,:));
        disp(strcat('The SIF for the current case is: ', ...
                    num2str(K1),' MPa sqrt(mm)'))
        test_I(f)=K1(1,1);
    end
    if mode==2
        Kx=overdcII_nt(matrix(1,:),matrix(2,:),matrix(3,:),...
                        matrix(4,:),n);
        disp(strcat('Terms used in the series expansion, n=' ,...
                    num2str(n)))
        disp(strcat('The SIF for the current case in mode I is, KI= ' ,...
                    num2str(Kx(1,1)),' MPa sqrt(mm)'))
        disp(strcat('The SIF for the current case in mode II is, KII = ' ,...
                    num2str(Kx(1,4)),' MPa sqrt(mm)'))
        test_I(f)=Kx(1,1);
        test_II(f)=Kx(1,4);
    end
    if actd_series==1
        % this part of the code fits a linear equation to the the
        % calculated SIF values in order to define the correction to apply
        % in the next run
        opts = fitoptions ('method', 'linearLeastSquares');
        [fit_SIF1,gof]=fit(order_of_terms',test_I','poly1');
        coef_SIF1=coeffvalues (fit_SIF1);
        [fit_SIF2,gof]=fit(order_of_terms',test_II','poly1');
        coef_SIF2=coeffvalues (fit_SIF2);
        ctc_vector(contador)=ctc(1);
        incl_vector(contador)=coef_SIF1(1);
        ctc_vector2(contador)=ctc(2);
        incl_vector2(contador)=coef_SIF2(1);
        % defintion of the correction to apply to the assumed crack tip
        % 5 corrections are calculated and afterwards the right correction
        % is determined through interpolation
        if contador < contador_max-1
            ctc(1)=ctc(1)+ctc_inc;
        elseif contador == contador_max-1
            figure(2000)
            plot(ctc_vector,incl_vector,'-o');
            ctc(1)=interp1(incl_vector,ctc_vector,[0],'pchip');
        elseif contador ==round(contador_max-1)
            break
Appendix C. SIF determination source code

```matlab
contador = contador + 1;

end

end

% plots the calculated SIF as a function of the number of terms used for
% calculation
if plot_figures == 1
    figure(1000+contador), hold on
title(strcat('corrX=', num2str(ctc(1)), '; corrY=', num2str(ctc(2))));
set(gca,'FontSize',16)
if mode == 2
    [figax, figKI, figKII] = plotyy(order_of_terms, test_I, order_of_terms,...
        test_II);
elseif mode == 1 || mode == 0
    [figax, figKI, figKII] = plotyy(order_of_terms, test_I, order_of_terms,...
        test_I);
end
set(figKI,'LineStyle','--','Color','r','Marker','square','MarkerSize',6,...
    'LineWidth',1)
set(figKII,'LineStyle','-.','Color','b','Marker','o','MarkerSize',6,...
    'LineWidth',1)
legend([figKI, figKII], 'K_{I}','K_{II}')
axes(figax(1))
set(gca,'FontSize',16,'YColor','k');
xlabel('number of terms in series expansion')
ylabel('K_{I}\ [MPa \ \sqrt{mm}]','interpreter','latex');
axes(figax(2))
set(gca,'FontSize',16,'YColor','k');
xlabel('number of terms in series expansion')
ylabel('K_{II}\ [MPa \ \sqrt{mm}]','interpreter','latex');

% end

disp(' ')
disp(' ')
disp('-' * 60)

FINAL RESULT:

disp(strcat('The SIF for the current case in mode I is, KI= ',...
    num2str(mean(test_I)), ' MPa \ \sqrt{mm}'))

disp(strcat('The SIF for the current case in mode II is, KII= ',...
    num2str(mean(test_II(1:2))), ' MPa \ \sqrt{mm}'))
```
The aforementioned code calls several functions which are shown below. First the stress around the crack tip is calculated in function `overd_stressv7.m`. In this function, the crack tip has to be determined precisely, which is performed in the code `crack_tip_detection.m`. Finally, the code `overdeII_nt.m` calculates the stress intensity factor.

### C.0.3 overd_stressv7.m

The following input variables for this code are given by the main function:

- **filename** Full filename of the file with the necessary input data
- **ct** This variable tells the code if the crack tip is defined manually, or if it should be determined automatically. If set to 1, the crack tip is determined automatically based on the strain field discontinuity near the crack tip
- **ctc** The vector defines a correction applied to the determined crack tip. This variable is used in the automatic optimisation of the determined crack tip
- **should_plot** If set to 1, verification plots are made

Output variables:

- **matrix** Location and stress values of the points used for SIF determination

Below is the commented source code for Matlab:

```matlab
function [matrix] = overd_stressv7(filename, ct, ctc, should_plot)

% function which calculates the stress field necessary for the
% overdeterministic determination of the SIF based on the strain field
% measured by GOM ARAMIS
% Valentin, Pedro, 15.9.09
% the input variable "ct" tells the program if it should automatically
% detect the crack tip, or if manual detection should be used
% should_plot: if set to "1", some plots will be made, otherwise no plots
% will be made

if nargin == 0
    disp(strcat('Time used for calculation: ', num2str(toc), 's'))
end
```
Appendix C. SIF determination source code

15 [filename, pathname] = uigetfile('*.txt', 'Please select the input file');
16 if isempty(filename)
17     stop, clc
18 end
19 file = fullfile(pathname, filename);
20 ct=0;  % manual crack tip
21 should_plot=0;  % no plot
22 end
23 if nargin==1
24     file=filename;
25     ct=0;  % manual crack tip
26     should_plot=0;  % no plot
27     ctc=[0 0];  % no correction
28 end
29 if nargin==2
30     file=filename;
31     ct=ct;
32     ctc=[0 0];  % no correction
33     should_plot=0;  % no plot
34 end
35 if nargin==4
36     file=filename;
37     ct=ct;
38     ctc=ctc;
39     should_plot=should_plot;
40 end
41
42 fid10=fopen(file,'r');
43 i=1;
44 while 1
45     tline = fgetl(fid10);
46     if ~ischar(tline), break, end
47     str{i}=tline;
48     i=i+1;
49 end
50 fclose(fid10);
51
52 % Material properties for stress calculation
53 nu=str2double(str(9));
54 E=str2double(str(7));
55
56 if ct==0
57     % coordinates of the crack tip in the image
58     cracktip_img=str2num(str(13));
59     dist_centro_x=cracktip_img(1);
60     dist_centro_y=cracktip_img(2);
61 end
62
63 % reads the measured strain values
64 file_ex=str(2);
65 file_ey=str(3);
66 ex=dlmread(file_ex,':',3,0);
67 ey=dlmread(file_ey,':',3,0);
Appendix C. SIF determination source code

```matlab
% figure(1)
% plot3(ey(:,1),ey(:,2),ey(:,4),'.k')

if ct==1
  % coordinates of the crack tip in the image
  cracktip_img=crack_tip_detection(ey,should_plot); % auto crack-tip detection
  dist_centro_x=cracktip_img.x*ctc(1);
  dist_centro_y=cracktip_img.y*ctc(2);
end

% eliminates the Z coordinate which was not measured
ex(:,3)=[];
ey(:,3)=[];

% centers the coordinate system on the crack tip
ex(:,1)=ex(:,1)-dist_centro_x;
ey(:,1)=ey(:,1)-dist_centro_x;
ex(:,2)=ex(:,2)-dist_centro_y;
ey(:,2)=ey(:,2)-dist_centro_y;

% rotates the coordinate system for cracks on the left side
if strcmp(crack_side,'left')
ex(:,1)=-ex(:,1);
ey(:,1)=-ey(:,1);
end

% calculates variables needed for the overdeterministic method
overd_limitsX =str2num (str {19});
passo =str2double (str {17});
limite_y =(overd_limitsX (2) - overd_limitsX (1))/2;
coordx=overd_limitsX (1): passo: overd_limitsX (2);
coordy=-limite_y:passo:limite_y;
dist =ones (size(coordy ,1) , size(coordy ,2)); angle =dist;
for i =1: size(coordy ,1)
  for j =1: size(coordx ,2)
    dist(i,j)=(sqrt(coordy (1,i)^2+coordx (1,j)^2));
    angle(i,j)=atan(coordy (1,i)/coordx (1,j));
  end
end

% eliminates the area around the region of interest near the crack tip.
border=0.1; % defines how much more is calculated in relation to the region of interest in X and Y directions
ey_=ey;
ey_(find(ey(:,1)<overd_limitsX (1)-border),:)=[];
ey_(find(ey(:,1)>overd_limitsX (2)+border),:)=[];
ey_(find(ey(:,2)<limite_y-border),:)=[];
ey_(find(ey(:,2)>limite_y+border),:)=[];
ex_=ex;
ex_(find(ex(:,1)<overd_limitsX (1)-border),:)=[];
ex_(find(ex(:,1)>overd_limitsX (2)+border),:)=[];
ex_(find(ex(:,2)<limite_y-border),:)=[];
ex_(find(ex(:,2)>limite_y+border),:)=[];
```

ex_ (find(ex_ (: ,2)>limite_y+border,:))=[];

% stress calculation
sy_ (: ,1)=ey_ (: ,1);
sy_ (: ,2)=ey_ (: ,2);
% sy_ (: ,3)=sy_ (: ,3);
% sy_ (: ,3)=E.*(ey_ (: ,3)+nu.*ez_ (: ,3))/(1-nu^2).*100;
sy_ (: ,3)=E./*.ey_ (: ,3)-nu.*ex_ (: ,3))./100;

% interpolate the measured result cloud onto a regular grid
[xx_ , yy_]=meshgrid ( coordx , coordy);
sy__grid_strain = griddata ( sy_ (: ,1) , sy_ (: ,2) , sy_ (: ,3) , xx_ ,yy_,'linear');
sx__grid_strain = griddata ( sx_ (: ,1) , sx_ (: ,2) , sx_ (: ,3) , xx_ ,yy_,'linear');

% plotting for verification
if should_plot==1
    figure (1)
surf (xx_ ,yy_ , sy__grid_strain )
    view (0,90)
    colorbar
    figure (2)
surf (xx_ ,yy_ , sx__grid_strain )
    view (0,90)
    colorbar
end

% reformats the output data for input into the overdeterministic algorithm
for i =1: size (dist ,1)
    for j =1: size (dist ,2)
        alg_sx (1,(i -1)* size (dist ,1)+ j)= dist (i,j);
        alg_sx (2,(i -1)* size (dist ,1)+ j)= angle (i,j);
        alg_sx (3,(i -1)* size (dist ,1)+ j)= sx__grid_strain (i,j);
        alg_sy (1,(i -1)* size (dist ,1)+ j)= dist (i,j);
        alg_sy (2,(i -1)* size (dist ,1)+ j)= angle (i,j);
        alg_sy (3,(i -1)* size (dist ,1)+ j)= sy__grid_strain (i,j);
    end
end

matrix=[alg_sx;alg_sy(3,:)];

This code calls the function crack_tip_detection.m for crack tip determination.
Appendix C. SIF determination source code

C.0.4 crack_tip_detection.m

The following input variables for this code are given by the main function:

**ey** Stress field perpendicular to the crack face

**plotyes** If set to 1, verification plots are made

Output variables:

**crack_tip** Detected crack tip coordinates

Below is the commented source code for Matlab:

```matlab
function [crack_tip] = crack_tip_detection(ey, plotyes)
% Valentin, 16.04.2010
% this function will locate the crack tip based on the strain field around
% the crack.
% "ey" is the strain field as measured by ARAMIS perpendicular to the crack
% "plotyes": if this variable is set to 0, no plot is made, if it is set to
% 1, then a verification plot is made
% %%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%

disp('Determining the crack tip')

% eliminates the Z coordinate which was not measured and values outside of
% the region of interest
ey(:,:,3) = [];
ey(ey(:,:,2) > 3,:) = [];
% limits the region of interest
ey(ey(:,:,2) < -3,:) = [];

% interpolate the measured result cloud onto a regular grid
% the grid should have at least the same resolution as the input data
x = min(ey(:,1)):0.06:max(ey(:,1));
y = min(ey(:,2)):0.06:max(ey(:,2));
[xx,yy] = meshgrid(x,y);

ey_grid = griddata(ey(:,1), ey(:,2), ey(:,3), xx, yy, 'nearest');

% crack tip detection: first the surface gradient is calculated and
% afterwards the maximum variation of this gradient in Y direction is
% used as an indicator of the crack tip. The whole image is scanned in
% order to find the crack tip in this way

[px, py] = gradient(ey_grid, 0.06, 0.06); % gradient density

for i = 1:size(yy,2)
    crack_tip_aux_sup(i) = max(py(:,i));
    crack_tip_aux_inf(i) = min(py(:,i));
end
```

```
absdiffsoma = abs(diff(crack_tip_aux_inf)) + abs(diff(crack_tip_aux_sup));

% this part of the code decides whether to track a1 or a2 depending on the
% input data and checks where the strain starts to get higher than the
% average value far away from the crack tip
if mean(absdiffsoma(2:7)) > mean(absdiffsoma(end-5:end))
    limiar = mean(absdiffsoma(end-10:end))*12; % limit definition
    % ("*12" is a parameter)
    for i=length(absdiffsoma):-1:1
        if absdiffsoma(i)>limiar
            crack_tip.x=x(i);
            ii=i-10;
            break
        end
    end
elseif mean(absdiffsoma(2:7)) < mean(absdiffsoma(end-5:end))
    limiar = mean(absdiffsoma(2:10))*12;
    for i=1:length(absdiffsoma)
        if absdiffsoma(i)>limiar
            crack_tip_auto_x_aux(i)=i;
            crack_tip.x=x(i);
            ii=i+10;
            break
        end
    end
end

% detects the Y coordinate of the detected crack tip
y_detect_max = find(py(:,ii)==max(py(:,ii)));
if plotyes ==1
    figure(4), hold on
    surf(xx,yy,ey_grid)
end

% detects the Y coordinate of the detected crack tip
y_detect_min = find(py(:,ii)==min(py(:,ii)));
if plotyes ==1
    figure(4), hold on
    surf(xx,yy,ey_grid)
end

% detects the Y coordinate of the detected crack tip
y_detect = round((y_detect_max + y_detect_min)/2);

% detects the Y coordinate of the detected crack tip
y_detect = y(y_detect);

if plotyes ==1
    figure(4), hold on
    surf(xx,yy,ey_grid)
end

% detects the Y coordinate of the detected crack tip
auto=plot3([crack_tip.x crack_tip.x],[10 10],[10 10],g,'LineWidth',2);
plot3([-5 20],[crack_tip.y crack_tip.y],[10 10],g,'LineWidth',2)
set(gca,'FontSize',16)
e

C.0.5 overdcII_nt.m

The following input variables for this code are given by the main function:

r_data radial distance to the crack tip of each point
**t_data** angle in relation to the crack tip of each point

**sx_data** stress parallel to the crack face of each point

**sy_data** stress perpendicular to the crack face of each point

**n** Number of terms to be used in the series evolution

Output variables:

**output** Calculated SIF in mode I and II based on the stress field in xx and yy directions

Below is the commented source code for Matlab:

```matlab
function [output] = overstII_nt(r_data, t_data, sx_data, sy_data, n)
% function for calculation of the SIF by an overdeterministic method
% translation into Matlab from the algorithm in Mathematica developed by S.
% Pastrama
% S. Pastrama, Pedro, Valentin, 04.08.09
% %
% % r_data: vector with the radius measured from the crack tip of each
% % measurement point
% % t_data: vector with the angle measured from the crack tip of each
% % measurement point
% % sx_data: vector with the stress parallel to the crack direction for each
% % measurement point
% % sy_data: vector with the stress perpendicular to the crack direction for each
% % measurement point
% % n: number of terms used for the series expansion

% %%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%%
% define the symbolic variables and calculate exactly as the Mathematica
% algorithm
syms r sy t K1 K2 pp pi_ sx

for nn = 3:n
A(nn) = sym(strcat('A_', num2str(nn), '));
B(nn) = sym(strcat('B_', num2str(nn), '));

% % original from S. Pastrama
% f(nn) = nn/2*r^((nn/2-1)*t)*(A(nn)*...
% ((2(-(1)^nn-nn/2)*cos((nn/2-1)*t)+(nn/2-1)*cos((nn/2-3)*t))...
% -B(nn)*...
% ((2+(1)^nn-nn/2)*sin((nn/2-1)*t)+(nn/2-1)*sin((nn/2-3)*t))...
% );

% part for sigma_x according to Sakaue2007aa.pdf
fx(nn) = (...)
A(nn)*nn/2*r^((nn/2-1)*t)...
((2-(1)^nn-nn/2)*cos((nn/2-1)*t)-(nn/2-1)*cos((nn/2-3)*t))...
-B(nn)*nn/2*r^((nn/2-1)*t)...
((2-(1)^nn-nn/2)*sin((nn/2-1)*t)-(nn/2-1)*sin((nn/2-3)*t))...
```
Appendix C. SIF determination source code

```matlab
f(nn)=(...
A(nn)*nn/2*r^((nn/2)-1)*
((2+(-1)^nn-nn/2)*cos((nn/2-1)*t)+(nn/2-1)*cos((nn/2-3)*t))...
-B(nn)*nn/2*r^((nn/2)-1)*
((2-(-1)^nn-nn/2)*sin((nn/2-1)*t)+(nn/2-1)*sin((nn/2-3)*t))...
);
end
ff=f(3); for i=4:n, ff=ff+f(i); end
ffx=fx(3); for i=4:n, ffx=ffx+fx(i); end

% original from S. Pastrama
% g= K1 / sqrt (2*( pi_ )* r )* cos (t/2)*(1+ sin (t/2)* sin (3* t/2))...
% -K2 / sqrt (2*( pi_ )* r )* sin (t/2)*(2+ cos (t/2)* cos (3* t/2))...
% +ff-sy;
% for sigma_x according to Kobayashi1979aa .pdf
% gx= K1 / sqrt (2*( pi_ )* r )* cos (t/2)*(1- sin (t/2)* sin (3* t/2))...
% -K2 / sqrt (2*( pi_ )* r )* sin (t/2)*(2+ cos (t/2)* cos (3* t/2))...
% +ffx-sx;

% for sigma_y according to Kobayashi1979aa .pdf
% g= K1 / sqrt (2*( pi_ )* r )* cos (t/2)*(1+ sin (t/2)* sin (3* t/2))...
% +K2 / sqrt (2*( pi_ )* r )* sin (t/2)*(2+ cos (t/2)* cos (3* t/2))...

% according to Sakaue2007aa .pdf
% g= K1 /(2* sqrt (2*( pi_ )* r ))*(5/2* cos (-t/2) -1/2* cos (-5* t/2))...
% +K2 /(2* sqrt (2*( pi_ )* r ))*(1/2* sin (-t/2) -1/2* sin (-5* t/2))...
% +ff-sy;

% based on sigma_x:
% cx=[diff(gx,K1),diff(gx,K2)];
ii=3;
conta=0;
regA=[];regB=[];
for i=3:n
if diff(gx,A(i))~=0
 cx(ii)=diff(gx,A(i));
 conta= conta+1;
 ii=ii+1;
end
regA=[regA i]; end
```
ii = 3 + conta;
for i = 3:n
    if diff(gx, B(i)) ~= 0
        cx(ii) = diff(gx, B(i));
        ii = ii + 1;
        regB = [regB, i];
    end
end
for i = 1:size(cx, 2)
    ccx(i,:) = subs(cx(i), {r, t, sx, pi_}, {r_data(1,:), t_data(1,:),...
        sx_data(1,:), 3.1416});
end
val_ini = ones(1, size(cx, 2));

% define the function ggx
ggx = subs(gx, {r, t, sx, pi_, K1, K2, A(regA), B(regB)}, ...
    {r_data(1,:), t_data(1,:), sx_data(1,:), 3.1416, val_ini(1), val_ini(2),...
    val_ini(3:3 + length(regA) - 1),...
    val_ini(3 + length(regA):3 + length(regA) + length(regB) - 1)});
b = ccx';
g1 = -ggx;
h1 = ccx * g1';
h2 = ccx * b;
j = h2 \ h1;

val_ini = val_ini + [j'];
% output the results nicely
resultadoK1 = val_ini(1);
resultadoK2 = val_ini(2);

% based on sigma_y:
c = [diff(g, K1), diff(g, K2)];
ii = 3;
conta = 0;
regA = []; regB = [];
for i = 3:n
    if diff(g, A(i)) ~= 0
        c(ii) = diff(g, A(i));
        conta = conta + 1;
        ii = ii + 1;
        regA = [regA, i];
    end
end
ii=3+conta;
for i=3:n
    if diff(g,B(i))~=0
        c(ii)=diff(g,B(i));
        ii=ii+1;
        regB=[regB i];
    end
end
for i=1:size(c,2)
    cc(i,:)=subs(c(i),{r,t,pi_},{r_data(1,:),t_data(1,:),sy_data(1,:),3.1416});
end
val_ini=ones(1,size(c,2));
gg=subs(g,{r,t,pi_,K1,K2,A(regA),B(regB)},
    {r_data(1,:),t_data(1,:),sy_data(1,:),3.1416,val_ini(1),val_ini(2),
    val_ini(3:length(regA)-1),...
    val_ini(3+length(regA):3+length(regA)+length(regB)-1)});
b=cc';
g1=-gg;
h1=cc*g1';
h2=cc*b;
% original version from Mathematica algorithm
% h3=inv(h2);
% j=h3*h1;
% Matlab version to solve the system of equations which is considerably
% faster and more accurate (theoretically)
% see matlab user manual for inv() function: Using A\b instead of inv(A)*b
% is two to three times as fast and produces residuals on the order of
% machine accuracy, relative to the magnitude of the data.
% condition=rcond(h2)  % if near 1, the matrix is well conditioned,
%                      % if near 0, results should not be trusted
j=h2\h1;
val_ini = val_ini + [j'];
% output the results nicely
resultado.K1=val_ini(1);
resultado.K2=val_ini(2);
% saves the calculated SIF as an output variable for the function for
% further processing

Appendix D

Device for clamping force measurement

One rarely studied parameter in FSW is the clamping force used during the welding process. This is however an important aspect, since for a more industrial application of the FSW process, automatic clamping systems are needed. It has to be known how much force such an automatic clamping system has to apply to the weld. Furthermore, the clamping of plates represents an important aspect for the modelling groups. More realistic boundary conditions may be defined by the information expected from this clamping system. The clamping system presented in this Appendix was built with the goal of measuring both the initial clamping force and its evolution during welding.
D.1 Traditional clamping system

Figure D.1 shows the original clamping system used in the Neos Tricept 805 robot at Department of Solid State Joining Processes at HZG (WMP), HZG. The clamping force is provided by bars screwed to the robot table. There is only a very limited control of the actual applied clamping force and its location though the torque applied to the screws of the clamping system. For practical applications this is however not precise enough, since very small differences in torque can lead to high differences in the applied force. Furthermore, no possibility exists to use the DIC based GOM mbH Aramis full field strain measurement system in the centre of the plates where less border effects are expected.

With this system no significant control over the clamping force is provided, and therefore the measured distortions and residual stresses may not be directly related to known welding parameters.

D.2 Specification of the device

A clamping system was conceived which measures the clamping forces in Z and X directions, where Z is the plate thickness direction, vertical to the table, Y is the welding direction, and X is the direction perpendicular to the welding line. Both the initial clamping force and its evolutions should be acquired. The positioning of the different
clamps should also be defined in order to be able to perform repeatable weldings. Some important aspects to be considered for the design of this new clamping included:

- An unobstructed line of sight to the centre of the plate was necessary, in order to allow the use of DIC based surface strain measurement systems, such as Aramis by GOM mbH.
- Clamping forces were expected to be as high as 2500N.
- The setup of the clamping system should be sufficiently simple to allow its usage in a great number of welds with different users. Therefore the setup should be almost self-explaining.
- The main task of the clamping system was to precisely define the initial clamping force applied to the specimen. Additionally the evolution of the clamping forces during the weld should be recorded.

D.3 Design variants

In this section, some of the studied design alternatives are presented, explaining their advantages and disadvantages. A difficult equilibrium between executability, cost, production time and precision had to be achieved.

Two main design alternatives were studied using piezoelectric load cells. The first one was based on uniaxial load cells, and the second one on three dimensional load cells. The final system shown in the section D.4 was based on resistive strain gauges.

D.3.1 Design alternative A

The first clamping system alternative was designed based on unidirectional load cells. At an approximate cost of 1000EUR per axis, and needing only 2 axes, this seemed to be an interesting alternative to 3D load cells. Figure D.2 shows the preliminary scheme of this clamping system for horizontal direction force measurement only. The vertical forces were not yet considered in this scheme.
Appendix D. *Device for clamping force measurement*

Figure D.2: Initial scheme for the clamping system with force measurement.

As can be seen, the clamping force would be applied mechanically and load cells would measure the applied force. The clamping in vertical direction was not defined in this stage, but vacuum clamping was deemed an interesting possibility, due to the free space on the upper surface for instrumentation. Figure D.3 shows the 3D Computer Assisted Design (CAD) drawing executed in Dassault Solidworks of this clamping device with a mechanical clamping variant for applying the vertical force. Bearings at the lower plate surface should reduce friction, so that vertical and horizontal clamping forces may be measured independently.

Figure D.3: CAD drawing of the clamping system - design alternative A.

Additionally to custom made parts and therefore workshop time, this clamping system would require the following material:

- 8 Kistler load cells Typ 9323A (≈1000EUR/cell)
Appendix D. Device for clamping force measurement

- 10 Bosch-Rexroth R0530 130 10 ball transfer units
- 4 INA-FAG RUE-25-D-FE linear guides and carriages

Two 20kN Kistler load cells on each side of the welding line measure the clamping force in the horizontal direction, furthermore two similar load cells measure the clamping force in vertical direction directly in the clamping area. The use of standard parts should assure that the building effort was low and that the parts may be reused for future devices if needed. Anyway a long manufacturing time would have to be planned due to the necessary precision of all parts.

While the system seems to be fairly simple in its construction and usage, it seems very difficult to guarantee a sufficiently high precision. Therefore the high cost of the system is not justifiable and further alternatives were designed.

D.3.2 Design alternative B

In order to achieve a higher precision, an alternative based on only four instead of eight load cells was developed. Three-dimensional Typ 9067C load cells were selected for this task. Figure D.4 shows a drawing of this clamping system alternative.

![Figure D.4: Force measuring clamping system using four 3D load cells.](image)

This clamping system measures the vertical clamping force indirectly, which means that it has to be calibrated. This is necessary, since there is not enough space below the robot for a sufficiently stiff load cell installation.

Figure D.5 shows more drawings of this clamping system. The complex design better guarantees the isolation of horizontal and vertical forces, but the setup of the system is more time consuming and therefore less practicable for a high number of welds.
The complexity of this design is clearly visible in Figure D.5(b). This is mainly due to the effort to reduce the influence of the vertical clamping force on the horizontal clamping force due to friction. While this approach seems to lead to an interesting solution, the prohibitively high production times and cost and the complexity of the setup is found to be excessive. A more simple approach was therefore taken, even if at the cost of some abilities.

\section*{D.4 Final design}

The final design of the clamping force measuring device was strongly based on the K.I.S.S. design principle, guaranteeing that the system could be built in an adequate time frame and at reduced cost. This leads to a first approach on the system which may be used thoroughly in order to gather initial information on the necessary clamping forces. Additionally due to its simplicity, the setup is very easy and the system is easily understandable by new users. This system may be used as a basis for future, more complex, designs if needed.

One advantage is the reduced cost of the system. Not only are there less mechanical parts which are also less complex, but the cost of the strain gauges at 5 to 10 EUR for each of the eight sensing devices is negligible in comparison to the 1000 EUR/axis price of the piezoelectric load cells, which would lead to an added cost of at least 8000 EUR. Furthermore, the required stiffness of the present solution is much lower than in the case of piezoelectric load cells, which reduced the size of the clamping system. Figure D.6 shows the final version of the clamping system, including a reference to the load application points and directions.
Appendix D. *Device for clamping force measurement*

4 vertical clamps
4 horizontal clamps
4 horizontal load cells
welding table with backing bar
4 vertical load cells
4 PTFE covered pads for load distribution

(a) Overview

(b) Magnification of the clamping system showing the load application points and directions

**Figure D.6:** Clamping force measuring device prepared for welding.
Appendix D. Device for clamping force measurement

The developed system is based on linear elastic beam equations. Strain gauges are used as load sensing devices on this system. While this approach is not as accurate as using piezoelectric load cells, the achieved precision of approximately 5% is still good enough for the demanded purpose. The approach presented hereafter was deemed to have the best ratio between obtainable results, cost and effort.

D.4.1 Hardware

Most of the parts used in this system are catalogue parts, easily replaceable and with a short delivery time. Only the load sensing elements were produced to the required specifications. Figure D.7 shows photos of these load sensing devices.

![Vertical force sensing device and Horizontal force sensing device](image)

**Figure D.7:** Pictures of the developed load sensing devices.

The measurement of the clamping force is done by 8 strain gauges. The acquired signals are then interpreted by the provided software. Figure D.8 shows the devices installed on the robot table.

The horizontal clamping devices (green) are instrumented with one strain gauge in the centre of the upper surface. A horizontal force applied on the clamp leads to a bending deformation which can be measured as a tensile strain on top, see Figure D.9(a). The vertical clamping bars are instrumented on the bottom surface near the hole for the screw which is used to tighten the system as shown in Figure D.9(b).
Figure D.8: FSW clamping system.

(a) horizontal clamping device

(b) vertical clamping device

Figure D.9: FSW clamping system - strain gauge location.

All strain gauges are installed according to the manufacturers specifications and protective coating is applied for their long term protection. Strain gauges from HBM with a gauge length of 6mm are used in the vertical clamping bars, while a 3mm long gauge length is used for the horizontal clamping devices, since this has proven to provide the highest accuracy possible.

While the grey parts in the Figure D.8 represent already existing parts or stock parts bought from suppliers, only the blue, yellow and green parts have to be machined in the workshop which considerably accelerates the production process.
Among the green horizontal clamping devices, the best size was chosen experimentally from different versions. The chosen part does not deform plastically during the welding process, but it deforms sufficiently in the elastic regime, so that strain can be measured on its top.

It should be noted that if the setup of the system, including the position of parts of the clamping system is changed, a new calibration should be performed, or at least should the existing calibration be verified. Figure D.10 shows the clamping system after setup and prepared for welding.

![Clamping system after setup and prepared for welding](image)

**Figure D.10:** Clamping force measuring device prepared for welding.

### D.4.2 Software

A Graphical User Interface (GUI) for controlling the clamping process and acquiring data was also developed. The main aim was to provide the end user with a simple to use and aesthetically pleasing, but fully functional interface. No manual should be needed for its operation in order to prevent the most common problems with software and measuring devices.

Due to license availability the software was made using the National Instruments (NI) LabView environment.
Appendix D. Device for clamping force measurement

The interface is divided into three main parts. In the first window, calibration of the load cells may be performed, or one of the stored calibration tables may be loaded. In the second window the gathered information is used in order to allow force controlled clamping, by displaying the current clamping force compared to the desired value for each of the eight force measuring points. Before clamping, this window allows to zero the strain gauge based load cells for accurate measurement. The third window is used during welding. In this window, the data recording can be started and stopped and graphics display the currently measured clamping system reaction forces.

Finally, the acquired data is written to a ASCII file with the naming scheme “DATE_DA_PPP_AAA_SpecimenID.txt”,
where DATE is the date in the format YYYYMMDDHHmm, PPP is the project designation, AAA is the owner of the experiment and SpecimenID is the name of the specimen. This file is recorded at 10Hz and contains nine columns. The first column is the time as the number of seconds elapsed since the start of the epoch at 1 January 1970 00:00:00 Universal Time (UT). This measure is good for synchronising different recordings on a computer with the same system time, but requires the subtraction of the first measured time of the experiment in order to get the duration of the experiment. Columns 2 to 5 are the horizontal clamping forces and columns 6 to 9 are the vertical clamping forces as represented in Figure D.11 starting from top left and ending on the lower right.

![Figure D.11: GUI showing the nominal and actual clamping forces while clamping the specimen (Designation of the clamping devices in the output file).](image)

Usually the welding is done from right to left in this scheme with a clockwise rotation of the tool. The top sensors are therefore usually on the retreating side and the bottom sensors on the advancing side of the weld.
D.4.3 Calibration

Calibration is performed by loading the clamping force measurement devices while measuring the applied load with a piezoelectric load cell. A table with information regarding the relation between changes in the resistivity of the strain gauges and the applied force is stored, which is afterwards used for calculating the force needed for a certain change in resistivity of the strain gauges.

Figures D.12 show the setup used for calibration. In the case of the vertical clamping bars, the load cell is located where the aluminium plate would normally be. For the horizontal clamping device, a different setup is needed due to load cell size restrictions. Care is taken in order to reproduce the real clamping conditions as precisely as possible.

![Vertical clamping force measuring device](image1)

(a) Vertical clamping force measuring device

![Horizontal clamping force measuring device](image2)

(b) Horizontal clamping force measuring device

**Figure D.12:** Calibration of the clamping force measuring devices.
During the calibration procedure, care should be taken not to enter the plastic zone of the load sensing devices. Therefore, after each small loading step, the step has to be unloaded again in order to verify elasticity. Figure D.13 shows the software interface used for calibrating the measuring devices.

![Clamping force measuring device calibration GUI.](image)

**Figure D.13:** Clamping force measuring device calibration GUI.

The light green marked sensor is being calibrated for the forces presented in the table to its right. The red marked area will contain the measured resistance value for each applied load. In the end the table containing information regarding the calibration values for different forces and all sensors is stored in a ASCII file. The calibration data is used for measuring the applied clamping force through a linear interpolation between the calibrated values. This procedure is valid, since the whole system is designed to work in the linear elastic regime only.

### D.4.4 Usage

The setup of this device is not noticeable more complex than the not controlled clamping system shown in Figure D.1. This is important, since it leads to a higher acceptance of the device, and therefore to a higher number of controlled welds. The clamping device requires that four arms are used for clamping instead of two, but the system could also be used with two arms if less detailed information is required. The main setup difference in relation to the original clamping system is that, while tightening the clamping system, the monitor has to be watched in order to stop at the correct clamping force. The well sized and colourful interface shown in Figure D.14 facilitates this task.
The clamping devices marked in blue have a lower clamping force than required, the red ones should have a lower clamping force and the green marked clamping devices are within 5% of the required clamping force value.

After finishing the clamping process, the only tasks to be performed are to click on the “welding” button in order to start the measuring module. In the next window, clicking on the “start” button records the forces until the “stop” button is clicked, see Figure D.15. While recording, an additional green light is turned on in order to inform the user.
Appendix D. Device for clamping force measurement

D.4.4.1 Influence of the vertical clamps on the horizontal force and weight of the bars

The influence of the frictional forces is reduced by using Polytetrafluoroethylene, commercially available as Teflon (PTFE) sheets between the vertical clamps and the aluminium plate to be welded. Nevertheless, frictional forces still exist in this setup.

The force of the vertical clamping was measured when only the own weight was being supplied to the plate. Only 25N were measured on each contact point on the plate ($4 \times 25N$). Figure D.16 shows the setup used for determining the frictional forces due to the vertical clamping system. One plate half was clamped with different vertical forces, and the horizontal load cells were used to push the plate out of its place while recording the necessary force.
Appendix D. Device for clamping force measurement

This setup allows an approximate measurement of the frictional force created by the vertical clamping system. The bottom side of the plate has frictional forces distributed on its area of approximately $0.15 \times 0.5 = 0.075 m^2$ between an aluminium vacuum table and the aluminium plate. The vacuum table only contacts the plate on $\approx \frac{1}{3}$ of this area due to its surface pattern. The top side of the plate has friction only on the contact zones with the clamping system lubricated with teflon sheets for reduced friction. The frictional area on the top is approximately $2 \times 0.015 \times 0.0055 = 1.65 \times 10^{-4} m^2$. Table D.1 shows the approximate horizontal force created due to the applied vertical force and its fractional value.

**Table D.1:** Influence of the vertical clamping force on the horizontal clamping force.

<table>
<thead>
<tr>
<th>vertical [N]</th>
<th>horizontal [N]</th>
<th>percentage [%]</th>
</tr>
</thead>
<tbody>
<tr>
<td>25</td>
<td>5 $\rightarrow$ 10</td>
<td>$\approx 20 \rightarrow 40%$</td>
</tr>
<tr>
<td>500</td>
<td>50 $\rightarrow$ 100</td>
<td>$\approx 10 \rightarrow 20%$</td>
</tr>
<tr>
<td>1000</td>
<td>150 $\rightarrow$ 200</td>
<td>$\approx 15 \rightarrow 20%$</td>
</tr>
<tr>
<td>1500</td>
<td>250 $\rightarrow$ 300</td>
<td>$\approx 17 \rightarrow 20%$</td>
</tr>
<tr>
<td>2000</td>
<td>350 $\rightarrow$ 400</td>
<td>$\approx 18 \rightarrow 20%$</td>
</tr>
<tr>
<td>2500</td>
<td>450 $\rightarrow$ 500</td>
<td>$\approx 18 \rightarrow 20%$</td>
</tr>
</tbody>
</table>

As can be seen, the horizontal clamping force in the current system will be equal to the applied horizontal clamping force plus approximately 20% of the applied vertical clamping force. The variation of the influence is partly due to the difference between the static and the dynamic coefficient of friction. Anyway, at least for the higher clamping forces, this difference is not significant. Since an almost constant relation exists between the vertical forces and the horizontally applied forces due to friction, the clamping force...
measurement device is capable of sufficiently isolating the effects of the vertical and the horizontal clamping devices for the planned tasks.

D.4.5 Limitations

Naturally, such a simplified device also has some limitations. First it is not possible to obtain such high precisions as would be possible with piezoelectric load cells or even when using more than one strain gauge on each of the load sensing devices. Secondly, the load applied by the weight of the clamping system itself is not measured and additionally the vertical clamping influences the horizontal clamping forces due to friction. However, these limitations are accepted as not influencing the work negatively.

D.5 Future developments

After having welded several specimens, some improvements for future systems were found to be of interest:

- Higher clamping forces, up to about 5000N should be possible. During the welds, it was found that the evolution of the initial clamping forces lead to maximum values of about 4000N almost independently of the initial clamping force value. Therefore a higher initial clamping force should be tried out.

- A clamping system could be conceived which allows not only to measure, but to actively control the clamping forces during welding, for example keeping a constant force during the whole weld. A complex servo-hydraulic controlled clamping system would probably be needed for this task.

- An automated, for example pneumatic, clamping system could speed up the clamping process.
Appendix E

Device for crack length measurement

In this appendix, a crack growth measurement equipment is described which was built in order to be able to measure the crack length for uniaxial fatigue tests such as C(T) tests. The measurement equipment is based on moving microscopes connected to linear scales.

While the vertical movement device was made according to the plans of the equipment already available in the laboratory, the horizontal movement device was completely redesigned in order to reduce cost, allow a shorter manufacturing time and a more comfortable usage while guaranteeing the same degree of accuracy.

The device was conceived to allow fast movement and when required fine movement. Furthermore the number of standard and readily available parts was intentionally chosen to be high for cost and manufacturing time reduction. Aluminium was chosen as the material for the main structural elements in order to keep the weight acceptable and in order to reduce or eliminate corrosion problems. Only the guiding elements were produced out of hardened stainless steel. Figure E.1 shows the drawing of the horizontal movement device.
Appendix E. Device for crack length measurement

Figure E.1: Scheme of the crack measurement device.

The position of the moving microscope is determined by a digital scale from Mitutoyo of the digimatic series (572-464) with a measurement range of ±225mm. A Herter Peak field microscope with a minimum work distance of 36mm was chosen for crack tip detection, since good experience exists with this microscope for crack tip detection in the laboratory. Its magnification of 20× allows a good visibility, while maintaining an acceptable overview over characteristic specimen details such as the initial notch for reference. Figure E.2 shows the installed crack measurement device.

Figure E.2: Picture of the installed crack measurement device.
Appendix F

Comparison of CMMs and stereo vision based systems for distortion measurement

Different measurement techniques may be used for distortion determination. In this Appendix, a short comparison of two techniques used for the present work is performed. Coordinate Measuring Machine based results and GOM mbH Pontos stereo vision based results are compared.

For this purpose, results concerning a HSM specimen with dimensions $150 \times 150 \text{mm}^2$ were compared mainly qualitatively. Different specimens have been measured with both techniques, and therefore the absolute values should not be directly compared.

While the CMM technique is mainly concerned with high precision, it can not compete with the speed and versatility of the stereo vision based system. Measurements may be performed with a precision of a few microns, but only up to the size limit of the used machine, generally in the order of $1 \text{m}^3$. Contrary to this technique, the stereo-vision based techniques has difficulties to achieve precisions in the range of a few hundreds of mm even in ideal conditions. This vision based system however allows to measure the same geometry several times per second, and has almost no size limitations. For example complete airplanes may be measured.

While the CMM based technique generally is better applied on a very clean surface in order to take advantage of its precision, the vision based system requires markers to be bonded to the surface in most cases. This further reduces the precision, and longer overall measurement times should be expected due to the surface preparation with markers.
The noise level of the stereo vision based system is inherently higher than on the touch probe based system, and the ambient conditions may influence the measurement accuracy. This is generally not a problem for touch probe based systems, since these machines are normally installed in clean and temperature controlled rooms.

Figure F.1 shows measurements performed by both techniques on different specimens for reference. As can be seen, the CMM based technique allows to measure a very high number of points with high precision, while the stereo vision based technique has advantages for larger complex structures where reduced measurement time is preferred.

As a conclusion, both techniques have important applications, and the correct technique should be chosen for each case. In the present work for example, residual stress measurement related deformations have been measured by CMM due to the higher precision and accuracy. On the other hand, for measurement of the general distortion as a function of the clamping parameters during FSW, the stereo vision based system has been selected. This allowed to make the measurements in a sufficiently short time.
Appendix G

Comparison of DIC and resistive strain gauges for strain measurement

Before the DIC based strain measurement system is used for welding experiments, some preliminary experiments were performed in order to determine the quality that is possible to be obtained.

Two situations for strain measurement were validated. A simple bi-dimensional (2D) test at constant temperature and slow variation in an easy setup, and a more complex three dimensional (3D) setup including temperature variation. The first experiment was based on a tensile test, and the second on a FSW experiment. Comparison was based in both cases on electrical strain gauges which are widely known to provide good strain measurement results.
Appendix G. DIC versus resistive strain gauges for strain measurement

G.1 Equipment

As reference HBM DMS 1-LC13-6/350 strain gauges with a gauge length of 6 mm, a nominal resistance of $350\Omega \pm 0.35\%$ and a gauge factor of $2.61 \pm 1\%$ were used, bonded to the specimen surface by HBM X280 high temperature adhesive. For data acquisition the NI CompactRiu device was used with two cards, one NI-9237 and one NI-9236. The electrical strain gauges were connected using three wires to the Wheatstone quarter bridge for best temperature compensation of the cabling.

The DIC based measurement system Aramis was provided by GOM mbH. White MR Chemie - MR2000 Anti-Reflex L aerosol spray was used as a background, and for the stochastic pattern the MOTIP - Matt black paint was chosen.

G.2 2D measurements at constant temperature

The first verification was performed using an instrumented tensile test. Since this was a simple 2D experiment at constant room temperature, no difficulties were expected from the ambient conditions.

G.2.1 Experimental setup

Figure G.1 shows the specimen used for validation. Two strain gauges were bonded to the specimens surface, and afterwards white background with the stochastic pattern was applied for the DIC based system.

![image](image1.png)

(a) before applying the stochastic pattern
(b) ready for the experiment

**Figure G.1:** Specimen used for validation of the GOM mbH Aramis DIC based strain measurement system.
Figure G.2 shows the experimental setup, comprised of a Zwick/Roell tensile testing machine, the strain gauge acquisition system, GOM mbH Aramis and a strong lighting system.

![Experimental setup](image)

**Figure G.2: Experimental setup.**

### G.2.2 Results

The obtained results are shown in Figure G.3. Both the resistive strain gauges and the DIC based system performed well in this circumstances.

![Comparison between strain and relative error](image)

**Figure G.3: Comparison between the strain measured by electrical strain gauges and the DIC based system.**

As can be seen, the relative error gets smaller as the measured strain grows, which may have at lest two reasons. Firstly, the time-axis of the graphs was synchronised only after the experiment, since no synchronisation signal could be used between both equipments. Therefore the initial instants may be ore strongly affected by a small offset in time. Secondly, the sensibility of the DIC based system may not be high enough for
the very small strains at the beginning of the experiment, and lead therefore to higher errors. It should be noted that the strain sensitivity of the DIC based measurement device depends, among others, on the quality of the stochastic pattern and the field of view of the cameras, which may be optimised for smaller strain leading to better results if required.

G.3 3D measurements at variable temperature

The second part of the validation was performed during the FSW process, since the complicated thermal influence and the fact that a 3D measurement is required justified this additional effort. Due to their wide availability, two aluminium plates of the alloy AA6061-T6 were welded by a Neos Tricept 805 robot with a custom FSW welding head.

G.3.1 Experimental Setup

Four strain gauges were used for measuring the strain at a distance of 40 and 90mm from the weld centre line. At this distance, temperatures well below 200°C are expected. Two strain gauges are used in order to guarantee a reliable level for comparison at each distance, see Figures G.4 and G.5. Thermocouples next to the strain gauges allow the compensation of thermal strain.

**Figure G.4:** Experimental setup used for the comparison of Aramis to a strain gauge.
The GOM mbH Aramis measurements were performed around the strain gauges. After the welding, the values calculated by Aramis and measured by the strain gauges were compared.

FSW was performed on a Neos Tricept 805 Robot, using a mechanical clamping system. The welding parameters used were a advancing speed of 600mm/min, a rotational speed of 800rpm and a downforce of 8kN. Tilt angles were set to $\theta = 0^\circ$. The experimental setup can be seen in Figure G.6
G.3.2 Results

Self-temperature compensated strain gauges as the ones used in this experiment do not measure the thermal expansion of the aluminium specimen. The thermal output, that is, the resistance variation of the wires due to temperature, may be compensated by the curve provided by HBM. This thermal output is given by equation G.1 for the present strain gauges.

\[
\varepsilon_s(T) = -225.19 + 12.516 \times T - 6.455 \times 10^{-2} \times T^2 + 8.21 \times 10^{-5} \times T^3
+ 252.4 \times 10^{-9} \times T^4 - 8.85 \times 10^{-10} \times T^5
+ 0.0114 \times L \times (T - 20) \mu m/m \pm 0.6(\mu m/m)°C^{-1}
\]  
\tag{G.1}

Since no better information exists, thermal expansion G.3 may be added to the strain measured by the strain gauges for later comparison to the DIC based system which measures both the thermal and the mechanical strain without distinction.

\[
\varepsilon_{\text{thermal}} = \alpha \Delta T
\]  
\tag{G.2}

\[\alpha = 23.0 \times 10^{-6} /°C\]

Before the welding and after the plate has cooled down, the information provided by the DIC based system and the electrical strain gauges should be the similar. During the
Appendix G. DIC versus resistive strain gauges for strain measurement

welding process, due to the thermal component of the strain, results may appear to be different.

Figure G.7 shows the temperature evolution at the four measuring locations during the weld and the corresponding measured strain.

![Figure G.7: Thermal cycle and corresponding strain at the measurement locations.](image)

Figure G.8 shows the thermal output seen by the resistive strain gauges and the theoretical thermal expansion at the measurement locations approximated by the measured temperature times the coefficient of thermal expansion for the material.

![Figure G.8: Correction factors applied to the strain gauge results.](image)

Figure G.9 compares the strain measured at four points during the test after adding the calculated thermal expansion to the resistive strain gauge results to the DIC based strain calculation results.
Appendix G. *DIC versus resistive strain gauges for strain measurement*

The very high noise level found in strain measurements by the DIC system may be due to the low acquisition frequency of 1Hz on this system. The GOM mbH Aramis software uses consecutive images in order to calculate deformation variations. Since the strain varies at a very high rate during welding, the acquisition rate is not adequate for strain measurement in this case. Additionally, the testing conditions are not ideally suited to DIC based systems, since the cameras cannot be positioned perpendicular to the surface as it would be recommendable. Furthermore, fumes and vibration during welding may adversely affect the quality of the results.

G.4 Conclusions

Very good agreement was found for strains measured during a tensile test. The DIC based system does provide significantly more information as the resistive strain gauge in this case, while maintaining a high quality of the results. For such a setup the DIC based strain measurement system may therefore be recommended.

For the more complex case of a 3D measurement with variations in temperature and under more difficult ambient conditions however, the noise level and acquisition rate were not adequate for measuring strain with the required accuracy. Therefore the DIC based system should not be used for measuring strain during the FSW process, at least using the setup and equipment available in the present work.
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