

PRE-STRAIN EFFECTS ON FATIGUE CRACK PROPAGATION BEHAVIOUR OF A PRESSURE VESSELS STEEL

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Abstract

Structural members and components are commonly subjected to plastic deformation during manufacturing or installation processes. This pre-history of deformation, usually called *pre-strain*, may have a profound influence on the resistance against fatigue crack growth of a material.

The present study aims to investigate the effect of tensile plastic pre-strain on fatigue crack growth behavior (da/dN vs ΔK) of a P355NL1 pressure vessel steel.

For that purpose, fatigue crack propagation tests were conducted on specimens with three distinct degrees of pre-strain: 0%, 3% and 6%. Moreover, CT and CTS specimens were tested under pure mode I and mixed mode (I+II) loading, respectively, in order to study the influence of various loading conditions. Mean stress effects were also investigated by employing two different stress ratios: $R=0.05$ and $R=0.5$.

Contrary to previous studies, which applied the plastic deformation directly on the specimen, in this work the pre-straining operation was carried out prior to the machining of the specimens with the objective to minimize residual stress effects. In addition, the microstructures of the pre-strained and non-pre-strained P355NL1 steel were observed and compared.

Results revealed that, for the P355NL1 steel, the tensile pre-strain increased fatigue crack initiation angle and reduced fatigue crack growth rates in the Paris region. The pre-straining procedure had a clear impact on the Paris Law constants, increasing the coefficient and decreasing the exponent. In the low ΔK region, results indicate that pre-strain might cause a decrease in ΔK_{th} .

It was verified that the strain energy density model proposed by Huffman was able to predict satisfactorily strain-life curves, stress-life curves and Paris' region fatigue crack growth rates for the P355NL1 steel.

The conclusions of this research encourage the consideration of pre-strain on fatigue design methods.

Resumo

Os membros e componentes estruturais são, normalmente, sujeitos a deformações plásticas durante processos de fabrico ou instalação. Esta pré-história de deformação, denominada *pre-strain*, pode ter uma profunda influência na resistência à propagação de fendas de fadiga no material.

Este estudo pretende investigar o efeito das pré-deformações plásticas na propagação de fendas de fadiga (da/dN vs ΔK) do aço P355NL1 para reservatórios sob pressão.

Para este efeito, foram realizados testes de propagação de fendas de fadiga em provetes com três níveis distintos de pré-deformação: 0%, 3% e 6%. Para além disso, foram testados provetes CT e CTS em modo I puro e modo misto (I+II), respetivamente, com o objetivo de compreender a influência de diversas condições de carregamento. Foi também investigado o efeito da tensão média, usando duas razões de tensão: $R=0.05$ e $R=0.5$.

Contrariamente a estudos prévios, que aplicaram a deformação plástica diretamente no provete, nesta investigação a operação de pré-deformação foi executada antes da maquinagem dos provetes, com o objetivo de minimizar os efeitos das tensões residuais. Adicionalmente, foram observadas e comparadas as microestruturas do aço P355NL1 para as condições pré-deformado e virgem.

Os resultados deste estudo revelaram que, para o aço P355NL1, a pré-deformação de tração aumenta o ângulo de iniciação da fenda de fadiga e reduz as taxas de propagação de fendas de fadiga na região de Paris. A aplicação da pré-deformação teve um claro impacto nas constantes da Lei de Paris, aumentando o coeficiente e diminuindo o expoente. Na região de baixo ΔK , os resultados indicam que a pré-deformação poderá levar a uma diminuição de ΔK_{th} .

Foi verificado que o modelo de densidade de energia de deformação, proposto por Huffman, é capaz de prever satisfatoriamente as curvas de deformação-vida e tensão-vida, tal como as taxas de propagação de fendas de fadiga no regime de Paris, para o aço P355NL1.

Os resultados desta investigação incitam a consideração da pré-deformação em métodos de projeto à fadiga.

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Nomenclature

Latin

a	Crack length
Δa	Crack increment
b	Fatigue strength exponent
$\vec{b}^2$	Burger's vector
c	Fatigue ductility exponent
C	Paris law coefficient
CCS	Castillo-Canteli-Siegele
D	Damage
da/dN	Fatigue crack growth rate
E	Elastic modulus
f_u	Ultimate tensile strength
f_y	Upper yield strength
F	Force
F_{max}	Maximum force
F_{min}	Minimum force
J	J-integral
K	Stress intensity factor; Strain hardening coefficient
K_c	Material's fracture toughness
K_L	Limiting K_{max} value above which no detectable closure occurs
K_{max}	Maximum stress intensity factors
K_{min}	Minimum stress intensity factors
K_o	Constant
K_{op}	Stress intensity factor
K_r	Residual stress intensity factor
K_t	Stress concentration factor

K_I	Mode I stress intensity factor
K_{II}	Mode II stress intensity factor
K_{III}	Mode III stress intensity factor
K'	Cyclic strength coefficient
ΔK	Stress intensity factor range; Fatigue crack growth driving force
ΔK_{eff}	Effective stress intensity factor range
ΔK_{th}	Threshold stress intensity factor range
ΔK_{up}	Limit stress intensity factor range
l_0	Length of the tensile zone
$\Delta l_{theoretical}$	Theoretical final displacement
m	Paris law exponent
n, n'	Strain hardening exponent
N	Number of cycles
N_f	Number of cycles to failure
r	Polar coordinate (radius)
R	Stress ratio
t	Specimen thickness
U	Effective stress intensity ratio
U_d	Dislocation strain energy
U_e	Elastic strain energy density
U_p^*	Complimentary plastic strain energy density
ν	Poisson's ratio
W	Characteristic length; Specimen width
x	Fatigue crack growth rate
Y	Dimensionless parameter

Greek

α	Loading angle
γ	Curve fitting parameter; Fatigue crack growth rate exponent
δ	Degree of pre-strain
ε'_f	Fatigue ductility coefficient
ε_r	Elongation
$\varepsilon_{theoretical}$	Theoretical strain
θ	Polar coordinate (rotation)
ρ_c	Critical dislocation density
σ	Nominal stress
σ_a	Stress amplitude
σ_e	Fatigue limit
σ'_f	Fatigue strength coefficient
σ_m	Mean stress
σ_{max}	Maximum stress
σ_{min}	Minimum stress
σ_0	Flow stress
σ_{UTS}	Ultimate tensile strength
σ_y	Yield strength
$\Delta\sigma$	Stress range

ACRONYMS

ASTM	American Society for Testing and Materials
B/FEM	Boundary/Finite Element Method
CA	Constant amplitude
CT	Compact tension
CTS	Compact tension shear
DP	Dual-phase

FCG	Fatigue crack growth
FCGR	Fatigue crack growth rate
FEDER	Fundo Europeu de Desenvolvimento Regional
LCF	Low cycle fatigue life
LEFM	Linear-Elastic Fracture Mechanics
SWT	Smith-Watson-Topper
TRIP	Transformation induced plasticity
VA	Variable amplitude
VCCT	Virtual crack closure technique

1 Introduction

1.1 Motivation

This work is part of an ongoing project named SciTech (Science and Technology for Competitive and Sustainable Industries) which is an R&D project co-financed by Programa Operacional Regional do Norte (NORTE2020), through Fundo Europeu de Desenvolvimento Regional (FEDER). The research aims to develop advanced methodologies both for fatigue behaviour analysis and structural integrity assessment. The target material of this study is the P355NL steel, which is a low alloy carbon steel usually intended for service in welded pressure vessels.

It is known that cyclic loading causes fracture of a material at a stress well below the stress that leads to failure in a single application of a load. The progressive damage of a material due to the application of a repetitive load is usually referred to as *fatigue*. Estimates indicate that fatigue leads to approximately 90% of the failures in engineering structures and components (Campbell 2008). Pressure equipment, such as pressure vessels, is increasingly subjected to considerable dynamic loads which may trigger the necessity for fatigue assessments in these structures.

The manufacturing processes of pressure vessels involve sheet metal forming, which induce permanent plastic deformations in the material. Moreover, an additional cold plastic straining might take place during installation procedure (Donato and Cavalcante 2015). These plastic deformations preceding service are commonly referred in the literature as “pre-strain”. It has been reported that pre-strain often leads to significant changes in the behavior of the material, particularly in the resistance to fatigue crack growth. Both threshold of fatigue crack growth and crack closure effects might be altered (Nakajima *et al.* 1999, Vor *et al.* 2009, Donato and Cavalcante 2015).

Hence, it is relevant to conduct experimental tests on engineering materials applications to investigate the effect arising from pre-strain on the subsequent cyclic behavior. The results of such researches contribute to the improvement of fatigue design procedures of structural components through the consideration of the pre-strain. This improvement permits more

accurate lifetime predictions and avoids excessive safety factors, which enable a reduction of costs.

Furthermore, if the pre-straining operation shortens crack growth life, imprecise assessments may lead to accidents that with large economic costs might endanger persons' safety. An additional reason for evaluating the effect of plastic pre-straining on crack growth is the fact that it assists in the optimization of a thermomechanical treatment to obtain an enhanced crack growth resistance of the material.

Most studies evaluating the effect of pre-strain on fatigue crack propagation response induce the plastic deformation directly on the notched specimen. This generates a non-uniform residual stress distribution in the material. A relatively new approach consists of applying the plastic pre-strain prior to the machining of the specimens (Donato and Cavalcante 2015). In this investigation, the latter method was employed in an attempt to isolate (or minimize) the effect of the residual stresses and simulate a uniformly strained material.

Despite the intensive study devoted to this topic, some aspects are still not fully understood and further research is necessary.

1.2 Objectives

The main objectives of this research are:

- i) Determinate the fatigue crack propagation laws for different pre-strain levels for the P355NL1 steel;
- ii) Evaluate the fatigue crack growth threshold;
- iii) Analyze the fatigue crack growth models and propose modifications to account for pre-strain effect.

1.3 Organization of the thesis

This research is organized as follows. Chapter 2 presents a literature review of fatigue. It begins with a brief description of the history of fatigue, followed by a review of the main factors influencing fatigue damage. Next, basic concepts of fracture mechanics are presented. The following sections feature the state of the art on fatigue crack growth prediction models. The

chapter ends with an overview of previous studies on the effect of plastic pre-strain on fatigue behavior.

Chapter 3 approaches the experimental program of this work. The material, specimens, experimental setup and procedure are described in this chapter. The microstructure of the P355NL1 steel is investigated for the non-strained and pre-strained conditions.

In chapter 4, the results obtained from the experimental tests are displayed, analyzed and discussed.

In chapter 5, the Huffman's strain energy density based fatigue model is introduced. This model was applied to predict the strain-life curves, stress-life curves and fatigue crack growth rates for the P355NL1 steel.

Chapter 6 summarizes the main conclusions of this dissertation and presents suggestions for future work.

2 Literature Review

2.1 An overview of fatigue history

Fatigue comprises 180 years of history and numerous contributions. Schütz (1996), Schijve (2003) and Mann (2013) presented detailed reviews of the fatigue history. In this section, it is described some milestones relevant to this work.

Allegedly, the first investigation in fatigue of materials has been conducted in 1829 by the German mine engineer Wilhelm Albert, in order to understand the failure of iron chains occurred in the Clausthal mines. Albert built a test machine to apply cyclic loadings to the iron chains and, in 1837, he published the first results in fatigue testing (Schütz 1996).

In 1843, the British railway engineer William Rankine identified the distinctive characteristics of fatigue failure and realized the danger associated with stress concentrations in machine elements (Suresh 1998).

The term “fatigue” was introduced in 1854 by the British Braithwaite referring to the failure of metals under cyclic loads. In his work, Braithwaite (1854) described a variety of failures caused by fatigue in machine components. In the same period, a great number of railway accidents caused by fatigue incited interest in the phenomenon. Among them, it is noteworthy the Versailles accident occurred in 1842, where the fatigue failure on a locomotive axle caused the death of around 60 people (Smith and Hillmansen 2004).

One of the researchers interested in these topics, and particularly in the failure of railway axles, was August Wöhler. Between 1852 and 1869, Wöhler designed and constructed testing machines, which later he used to conduct fatigue tests in full-scale railway axles and structural components (Suresh 1998). In his investigations, Wöhler concluded that the stress amplitude is an essential parameter in fatigue life. In addition, he observed that the increase of the mean stress led to a smaller stress amplitude causing failure. Moreover, Wöhler introduced the concept of fatigue limit by discovering that a steel loaded cyclically possessed a limit value for the stress amplitude, below which infinite fatigue life could be achieved. The results of his fatigue tests were presented in a tabular form. Later, Spangenberg plotted the same results using

a linear scale, thus generating S-N curves that related the stress amplitude with the number of cycles until failure (Schütz 1996).

Basquin (1910) plotted the finite-life region of these curves using a log-log scale and found a linear relationship between $\log \sigma_a$ and $\log N_f$.

The German Heinrich Gerber (1874) and the British John Goodman (1899) investigated the mean stress effect on the fatigue resistance and developed methods for fatigue design (Suresh 1998).

In 1886, Johann Bauschinger identified the elastic limit alteration of metals subjected to cyclic loads. This phenomenon, designated “*Bauschinger effect*”, is the basis of the low cycle fatigue. Bauschinger works confirmed results previously expressed by Whöler and identified cyclic hardening and softening in metals. Kirsch calculated for the first time, in 1898, the stress concentration factor of 3.0 for a circular hole in an infinite plate (Schütz 1996).

In the beginning of the 20th century, Ewing e Humfrey used optical microscopy to carry out studies on mechanisms of fatigue. These investigators observed the development of slip bands on the specimens’ surface which led to the formation of cracks. This was the first metallurgical description of the fatigue phenomenon (Stephens *et al.* 2000).

In the 20s, various works of great importance for fatigue study emerged. In 1920, Alan Griffith showed the tensile strength of glass was influenced by the dimensions of microscopic cracks presented in the material. By using energetic considerations, Griffith established the condition for unstable crack propagation in a brittle solid, thus developing the basis of fracture mechanics (Stephens *et al.* 2000).

Gough and his associates gave relevant contributions for the understanding of fatigue mechanisms and characterized the effect of combined bending and torsion on fatigue strength. In 1924, Palmgren suggested a linear damage accumulation model for variable amplitude loading. Haigh (1917) and McAdam (1929) made corrosion-fatigue tests and noticed an important decrease in fatigue resistance resulting from corrosion in metals (Stephens *et al.* 2000).

In the subsequent years, mechanical methods with great practical importance were developed to improve fatigue strength by inducing compressive residual stresses. For example, the shot peening was implemented in the automotive industry in 1930 and reduced significantly the common failures of springs and axles (Stephens *et al.* 2000).

Gassner investigated fatigue strength under variable amplitude loading in aircraft structures. This German researcher was the first to develop a variable-amplitude fatigue test, which consisted in applying programmed sequences of load cycles with different amplitudes (Schijve 2003).

In 1945, Miner formulated mathematically a cumulative damage criterion based on the hypothesis suggested by Palmgren in 1924. This criterion is known as *Miner's rule* (or *Palmgren-Miner rule*) and, despite its limitations, represents an important tool for fatigue life prediction (Stephens *et al.* 2000).

In the 50s, the *Comet*, the first commercial airplane with jet propulsion, suffered numerous catastrophic crashes due to fatigue failure. These accidents led to the implement of mandatory full-scale fatigue tests in the aeronautical industry. Moreover, the “fail safe” philosophy was introduced in aircraft design (Stephens *et al.* 2000).

In 1954, Coffin and Manson verified that *low cycle fatigue* behavior was controlled by the plastic strain. The two investigators established an empirical relation between plastic strain amplitude and fatigue life named *Coffin-Manson law* (Stephens *et al.* 2000).

In 1957, George Irwin, based on the works of Griffith and Westergaard, demonstrated that the stress field near the crack tip can be expressed as a function of a scalar, termed stress intensity factor K . His work represents the basis of the linear elastic fracture mechanics (Suresh 1998).

In 1961, Paris, Gomez and Anderson correlated the stress intensity factor introduced by Irwin with the velocity of propagation of a crack through an equation known as *Paris law*. In the subsequent years, numerous relations of the same type were presented, in an attempt to overcome the limitations introduced by the *Paris law* (Schütz 1996).

In the decade of 1970, Wolf Elber emphasized the importance of the crack closure phenomenon in fatigue crack growth. In his experiments, Elber observed that the tip of fatigue crack in propagation could be closed when subjected to a tensile stress and concluded that such phenomenon was due to the plastic deformation left in the crack tip zone (Schijve 2003).

In 1970, Paris identified a minimum stress intensity factor range (ΔK_{th}) below which crack propagation would not occur (Stephens *et al.* 2000).

In June of 1974, the USA Air Force emitted the Mil A-83444, which specifies damage tolerance requirements for military aircraft design. This type of specifications led to an increased need for better non-destructive testing methods (Stephens *et al.* 2000).

In the decades of 80 and 90, various authors studied the complex problem of multiaxial fatigue (Brown and Miller 1973, Fatemi and Socie 1988, Glinka *et al.* 1995, Kandil *et al.* 1982). During

this period, it was verified that microcracks grew at low ΔK -values below the threshold limit defined for macrocracks ('small crack problem') (Schijve 2003). This phenomenon was studied in some investigations (Lankford 1982, Miller and De los Rios 1986, Pearson 1975, Tanaka *et al.* 1981).

2.2 Fatigue considerations

As mentioned earlier, the works of August Wöhler in the 1860s promoted the characterization of fatigue life in terms of stress amplitudes. The relation between the amplitude of stresses (σ_a) and the number of cycles to failure (N_f) is typically expressed via S-N curves (or Wöhler curves). The fatigue properties of materials described by the S-N curves are obtained through constant amplitude (CA) loading tests on un-notched specimens ($K_t = 1$). These tests are carried out until failure or until a high number of cycles, if failure does not occur.

Figure 2.1 shows characteristic stress levels of a load cycle that are defined by the Equations (2.1), (2.2), (2.3) and (2.4):

- Stress range: $\Delta\sigma = \sigma_{max} - \sigma_{min}$ (2.1)

- Stress amplitude: $\sigma_a = \frac{\sigma_{max} - \sigma_{min}}{2}$ (2.2)

- Mean stress: $\sigma_m = \frac{\sigma_{max} + \sigma_{min}}{2}$ (2.3)

- Stress Ratio: $R = \frac{\sigma_{min}}{\sigma_{max}}$ (2.4)

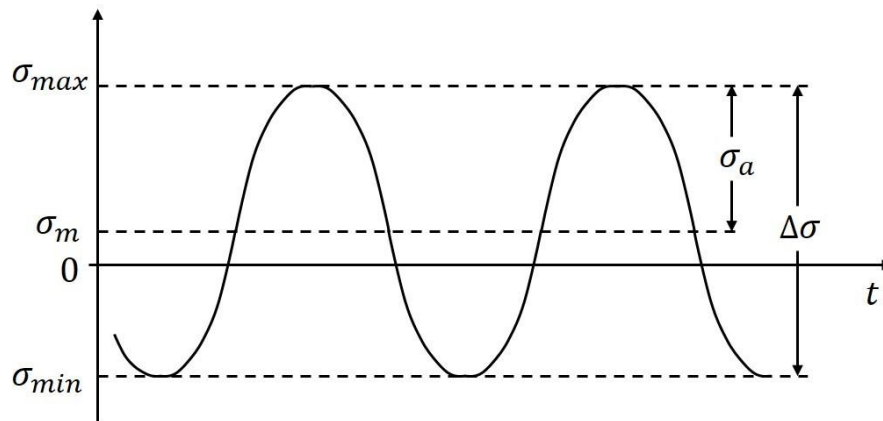


Figure 2.1 - Stress parameters of a constant amplitude load cycle

As may be seen in the S-N curve given in Figure 2.2, the cyclic life decreases with increasing stress amplitudes. Moreover, the stress-life plot exhibits a horizontal asymptote, below which the specimen is expected to have an infinite fatigue life (or greater than 10^6 cycles), usually referred to as fatigue limit σ_e . However, multiple high strength steels, aluminum alloys and other materials do not possess this horizontal plateau and stress amplitude that leads to failure continues to decrease for high fatigue lives. In these cases, the fatigue limit is determined as the stress amplitude supported by a specimen for at least 10^7 cycles.

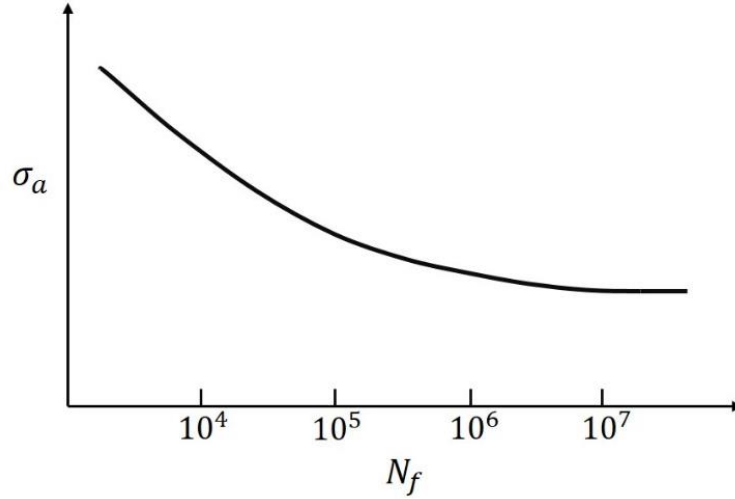


Figure 2.2 - Typical S-N diagram

Plotting the S-N curve on a bi-logarithmic scale leads to an approximately linear relation for a considerable range of N_f values, usually known as *Basquin relation* (Basquin 1910):

$$\frac{\Delta\sigma}{2} = \sigma_a = \sigma'_f (2N_f)^b \quad (2.5)$$

where σ'_f and b are the fatigue strength coefficient and the fatigue strength exponent, respectively.

Another important variable to describe a material's response to cyclic loading is the mean stress level (σ_m). A S-N curve is defined for a constant mean stress applied.

Figure 2.3 presents typical S-N plots for various mean stress levels. It is noted that the increase in the mean stress leads to lower S-N curves, which means that the fatigue resistance decreases with increasing stress levels, for a given σ_a .

Hence, it is convenient to express fatigue life data as a function of two stress variables: stress amplitude (σ_a) and mean stress (σ_m). For this purpose, some empirical relations, presented in Equations (2.6), (2.7) and (2.8), have been developed to evaluate the effect of mean stress on fatigue endurance (Gerber 1874, Goodman 1899, Soderberg 1939):

- Gerber relation: $\sigma_a = \sigma_e \left[1 - \left(\frac{\sigma_m}{\sigma_{UTS}} \right)^2 \right]$ (2.6)

- Goodman relation: $\sigma_a = \sigma_e \left[1 - \frac{\sigma_m}{\sigma_{UTS}} \right]$ (2.7)

- Soderberg relation: $\sigma_a = \sigma_e \left[1 - \frac{\sigma_m}{\sigma_y} \right]$ (2.8)

where σ_m is the mean stress, σ_{UTS} is the ultimate tensile strength, σ_y is the yield strength, σ_e is the fatigue limit for fully reversed loading ($\sigma_m = 0$ and $R = -1$) and σ_a is the fatigue limit for $\sigma_m \neq 0$

Such expressions can be extended to a more general approach by replacing the fatigue limit σ_e with a fully reversed ($\sigma_m = 0$ and $R = -1$) stress amplitude, corresponding to a certain fatigue life.

The previous relations are graphically expressed using constant life diagrams (Figure 2.4) to illustrate the combined influence of σ_m and σ_a on fatigue strength.

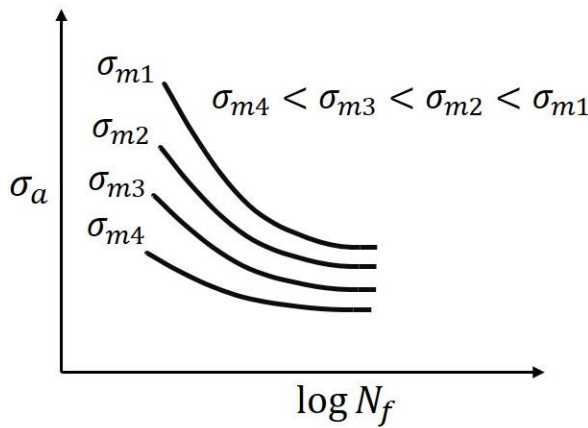


Figure 2.3 - Dependence of the S-N curves with the mean stress value

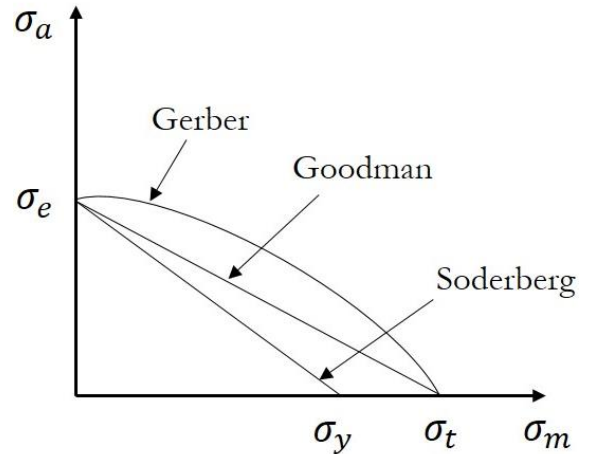


Figure 2.4 - Constant life diagrams

Morrow (1968) modified the Basquin relation to account for the mean stress effect, as it is shown in Equation (2.9):

$$\frac{\Delta\sigma}{2} = \sigma_a = (\sigma'_f - \sigma_m) N_f^b \quad (2.9)$$

Depending on the nature of the material, other factors may influence fatigue life. Among these are stress concentrations, residual stresses, surface conditions, environment and type of loading (Suresh 1998).

2.3 Crack opening modes

Assuming the presence of a crack in a homogeneous material subjected to a certain loading, there are three major crack opening modes:

- Mode I: Tensile opening mode, which corresponds to a tensile loading in the direction normal to the plane of the crack;
- Mode II: In-plane shear, in which the shear loading is applied perpendicularly to the crack front;
- Mode III: Transverse shear, which is associated with a shear loading parallel to the crack front.

The three different loading modes are illustrated in Figure 2.5. Note that each of these modes implies different crack surface displacements.

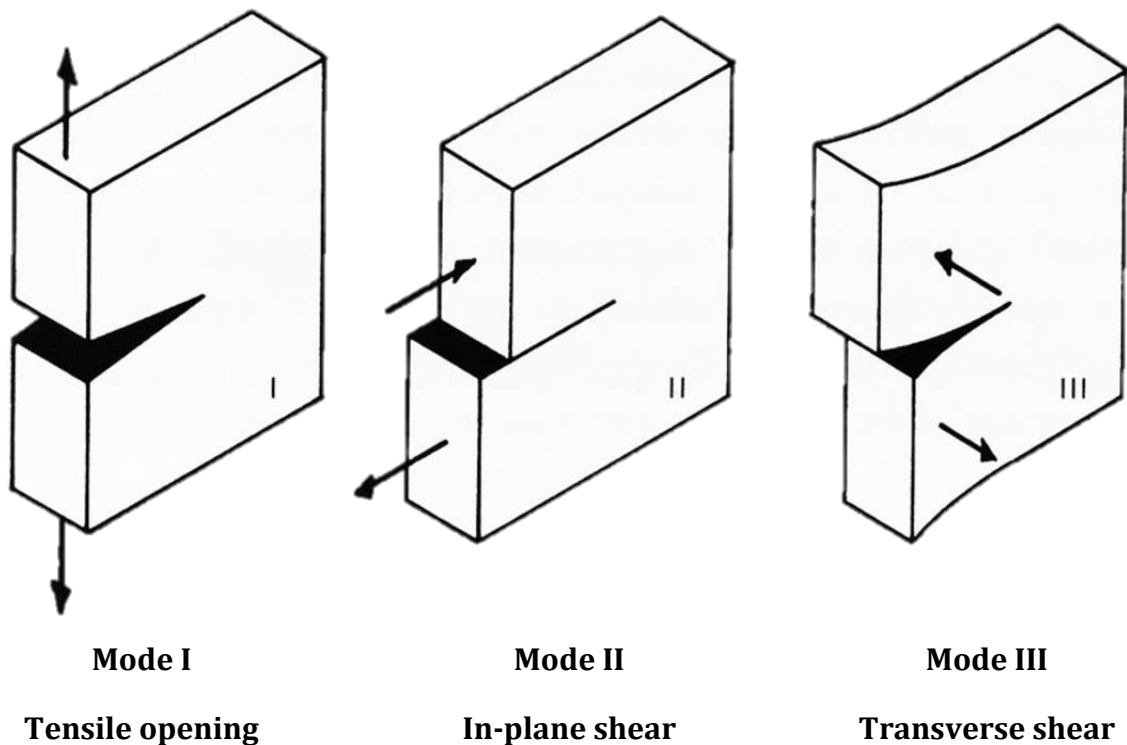


Figure 2.5 - The three crack opening modes [Chambel 2014]

Mode I is the most important loading mode in practical engineering applications. Therefore, it has received special attention in researches on fatigue crack growth. Nevertheless, mixed-mode loading conditions, which combine different modes of crack opening, are also very frequent.

2.4 Concept of stress intensity factor

In 1957, Irwin expressed, according to the notation shown in Figure 2.6, the elastic stress field near a crack tip in mode I loading as:

$$\begin{aligned}\sigma_x &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) \\ \sigma_y &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) \\ \tau_{xy} &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \sin \frac{\theta}{2} \cos \frac{3\theta}{2}\end{aligned}\tag{2.10}$$

where ν is the Poisson coefficient, r and θ are the polar coordinates and K_I is the mode I stress intensity factor.

In opposition, the stress field ahead of the crack tip under mode II loading was defined as:

$$\begin{aligned}\sigma_x &= \frac{-K_{II}}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \left(2 + \cos \frac{\theta}{2} \cos \frac{3\theta}{2} \right) \\ \sigma_y &= \frac{K_{II}}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \cos \frac{\theta}{2} \cos \frac{3\theta}{2} \\ \tau_{xy} &= \frac{K_{II}}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right)\end{aligned}\tag{2.11}$$

where K_{II} is the mode II stress intensity factor.

Finally, for mode III:

$$\begin{aligned}\tau_{xy} &= \frac{-K_{III}}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \\ \tau_{yz} &= \frac{K_{III}}{\sqrt{2\pi r}} \cos \frac{\theta}{2}\end{aligned}\tag{2.12}$$

where K_{III} is the mode III stress intensity factor.

From the expressions (2.10), (2.11) and (2.12), it is seen that the stress intensity factor is associated with a loading mode. It is also worth mentioning that the stress intensity factor is not dependent on the polar coordinates (r and θ) and therefore does not control the spatial distribution of the stress field. This parameter works rather as a scale factor that defines the magnitude of the crack tip stress field. An important feature of equations is the fact that the distribution of the elastic stress field in the vicinity of the crack tip is invariant for a determined loading mode.

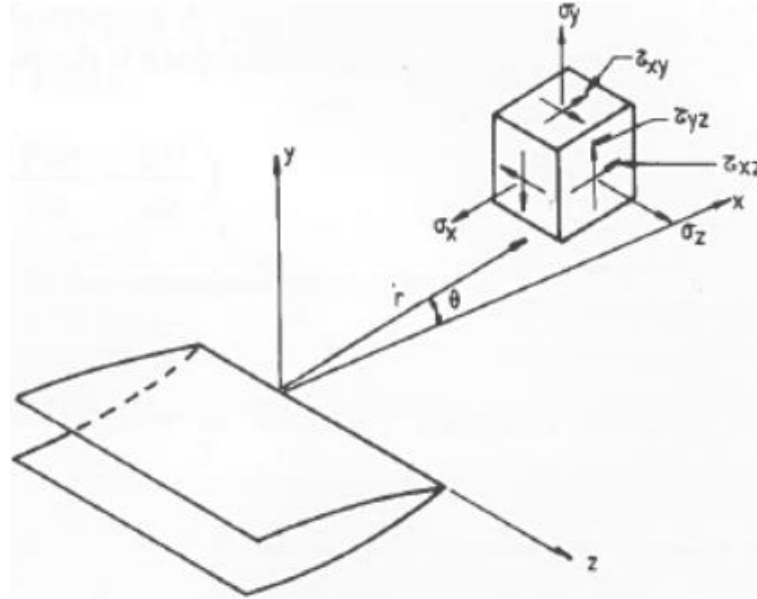


Figure 2.6 - Coordinate system and distribution of stresses in vicinity of the crack tip (Branco *et al.* 1986)

The value of K depends on a diversity of factors, namely crack length and location, geometry of the cracked component and loading. In general, the stress intensity factor can be expressed by the Equation (2.13):

$$K = Y\sigma\sqrt{\pi a} \quad (2.13)$$

where σ is the nominal stress applied, a is the crack length and Y is a dimensionless parameter which is function of the geometry and loading distribution (Branco *et al.* 1986). Solutions for K are available in the literature for a wide range of loading conditions and geometries (Tada *et al.* 1973, Rooke and Cartwright 1976, Murakami and Keer 1993). For complex configurations, it is often necessary to resort to experimental or numerical methods.

The assumption of linear elastic behavior leads to infinite stresses at the crack tip. According to Equations (2.10), (2.11) and (2.12), for $r \rightarrow 0$ the stress components tend to infinity. However, this is obviously not possible and instead, plastic deformation occurs at the crack tip. The size of the plastic zone created at the crack tip can be determined by using yield criteria, like Von Mises or Tresca. For small scale yielding conditions (i.e. for small plastic zone sizes), Linear Elastic Fracture Mechanics and the concept of stress intensity factor are valid (Suresh 1998).

2.5 Fatigue crack propagation models

The importance of crack growth in fatigue life evaluation encouraged the development of prediction models that reliably describe that phenomenon.

Paris *et al.* (1961) were the first to suggest the fracture mechanics concept of stress intensity factor (K) as the leading parameter in the characterization of fatigue crack growth process:

$$\frac{da}{dN} = f(\Delta K) \quad (2.14)$$

where da/dN is the fatigue crack growth rate (i.e. the crack advance in a single load cycle), f is a function to be defined and ΔK is the stress intensity factor range, defined by the Equation (2.15):

$$\Delta K = K_{max} - K_{min} \quad (2.15)$$

where K_{max} and K_{min} are the maximum and minimum stress intensity factors, respectively.

From the results of numerous crack growth tests, Paris and Erdogan (1963) established a relationship between the stress intensity factor range and the fatigue crack growth rate using a power law, which is represented by the Equation (2.16):

$$\frac{da}{dN} = C (\Delta K)^m \quad (2.16)$$

where C and m are experimentally obtained constants. This relation, known as *Paris law*, became a milestone in fracture mechanics and is used even today due to its simplicity.

However, for extreme values of the stress intensity factor range, the linear relationship between $\log da/dN$ and $\log \Delta K$, suggested by the *Paris law*, is not verified. This is an important limitation of the Paris relation. As shown in Figure 2.7, the fatigue crack growth rate curve displays a sigmoidal variation and may be divided in three distinctive regions:

- Region I: Near threshold region
- Region II: Paris region
- Region III: Unstable crack growth region

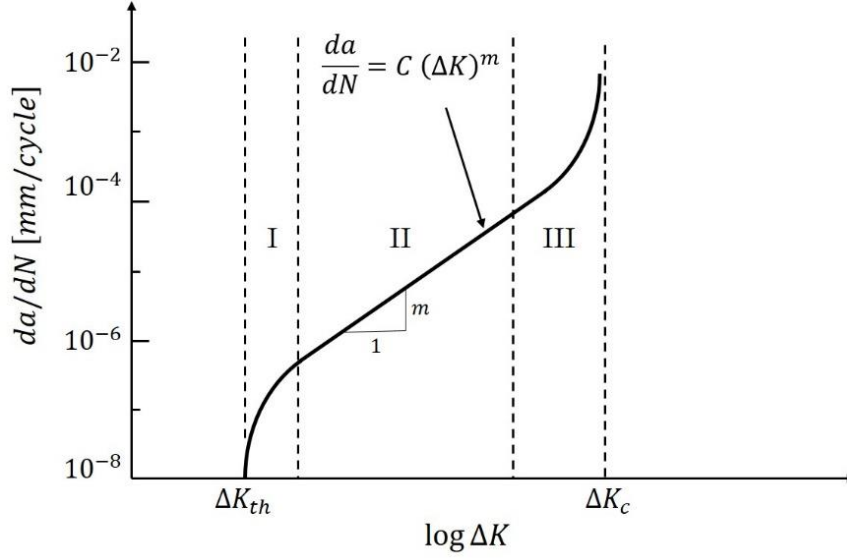


Figure 2.7 - Regimes of fatigue crack growth

Region I presents a vertical asymptote associated with a threshold stress intensity factor range (ΔK_{th}), below which fatigue crack propagation does not occur (or it occurs at undetectable rates).

Region II is an intermediate region of fatigue crack growth that shows a linear relationship between $\log da/dN$ and $\log \Delta K$. Thus, the Paris relation is valid in this region.

Region III is linked to rapid and unstable crack propagation that usually represents a small fraction of the total fatigue life. This region possesses a vertical asymptote for the critical value of K_{max} that leads to final failure ($K_{max} = K_c$).

Another important limitation of the Paris relation is the fact that it does not consider the effect of the stress ratio, whose influence on fatigue crack growth rate has already proven to be relevant (Schijve 2003, Suresh 1998). Tests have shown that different stress ratios lead to parallel fatigue crack growth rate curves, which means that the constant C varies with the stress ratio while the constant m remains the same (Beden *et al.* 2009).

Several alternative models have been proposed in an attempt to overcome these limitations.

Walker (1970) suggested a modification of the Paris law (Equation (2.17)) that considers the effect of the stress ratio:

$$\frac{da}{dN} = C \left[\frac{\Delta K}{(1 - R)^{1-\gamma}} \right]^m \quad (2.17)$$

where γ is a third curve fitting parameter added by this model. C and m are the parameters from the original Paris relation. For $R = 0$, Equation (2.17) becomes the Paris relation. If $\gamma = 1$, the

stress ratio has no influence on fatigue crack growth rate. Despite this improvement, the Walker model is still not able to describe the asymptotic behavior of regions I and III.

Forman (1972) proposed an empirical model valid for both medium and high crack growth rates, i.e. for regions II and III:

$$\frac{da}{dN} = \frac{C \Delta K^m}{(1-R)(K_c - K_{max})} \quad (2.18)$$

Note that da/dN tends to infinity as K_{max} approaches K_c , which is consistent with the right vertical asymptote previously referred. This model still describes the stress ratio effect on crack growth rate.

In order to account for the near threshold region (region I), Hartman and Schijve (1970) modified Forman's expression, as follows in Equation (2.19):

$$\frac{da}{dN} = \frac{C (\Delta K - \Delta K_{th})^m}{(1-R)(K_c - K_{max})} \quad (2.19)$$

Note that the new asymptote considered imposes that da/dN tends to zero as ΔK approaches ΔK_{th} , which is verified in Equation (2.19). Therefore, this model can describe the three crack propagation regions. On the other hand, the impact of the stress ratio in the value of ΔK_{th} is not considered by this relation.

Castillo *et al.* (2014) formulated a new approach (*CCS model*) that describes the crack growth rate curve by using a cumulative distribution function - Gumbel distribution:

$$\frac{\log \Delta K^* - \log \Delta K_{th}^*}{\log \Delta K_{up}^* - \log \Delta K_{th}^*} = \exp \left[- \exp \left(\frac{\alpha - \log \frac{da^*}{dN^*}}{\gamma} \right) \right] \quad (2.20)$$

where α , γ , ΔK_{th}^* and ΔK_{up}^* are parameters that may be estimated by the least-squares technique.

The normalized variables are identified with '*' in the upper index and are defined by:

$$a^* = \frac{a}{W}; \quad N^* = \frac{N}{N_0}; \quad \Delta K^* = \frac{K_{max} - K_{min}}{K_c}; \quad \Delta K_{up}^* = \frac{\Delta K_{up}}{K_c}; \quad \Delta K_{th}^* = \frac{\Delta K_{th}}{K_c} \quad (2.21)$$

where W is the characteristic length, N is the number of cycles, N_0 is the reference number of cycles, ΔK_{up} is the limit stress intensity factor range and K_c is the material's fracture toughness.

The expressions (2.21) characterize fatigue crack growth rate using the Linear Elastic Fracture Mechanics concept of stress intensity factor range (ΔK). Thus, they are only valid when the size of the plastic zone at the crack tip is small.

For large-scale yielding conditions, Dowling and Begley (1976) suggested the use of the J-integral as the primary parameter to characterize the rate of fatigue crack propagation, as it is shown in Equation (2.22):

$$\frac{da}{dN} = C \Delta J^m \quad (2.22)$$

From the observation of the above expression, it is evident the similarity with the Paris relation. Both relations describe region II of the fatigue crack growth curve and do not account for the stress ratio influence. The difference lays in the fact that the Paris relation is applicable when Linear Elastic Fracture Mechanics is valid, while Equation (2.22) can only be used for large-scale yielding conditions.

The previously discussed models have been developed to predict fatigue crack growth rates under constant amplitude (CA) loading.

In variable amplitude (VA) loading conditions, the interactions associated with the load sequence may either shorten or extend fatigue life considerably. For example, the occurrence of a tensile overload in the cyclic load (CA-loading) induces a delay in fatigue crack propagation, usually known as crack growth retardation.

Therefore, new models are necessary to express the effects of load history in fatigue crack growth.

Wheeler (1972) formulated a model that expresses the reduction in crack growth rate resulting from a peak overload. The Wheeler model uses a retardation factor that operates directly in the propagation law and accounts for the interference between the yield zone created by the overload and the plasticity zone induced by the current applied load.

Willenborg *et al.* (1971) proposed a retardation model based on the premise that crack delay after an overload is due to a reduction in K_{max} , corresponding to the current crack length. This model defines the amount of crack retardation by considering the stress intensity factor necessary to cancel the effect of the overload plastic zone.

The Wheeler model and the Willenborg model are not able to simulate crack growth acceleration resulting from compressive overload or delayed retardation induced by tensile-compressive overloads.

Various modified versions of these two models were proposed in order to extend their range of applicability and surpass their limitations. For example, the NASGRO model (or modified generalized Willenborg model) is an extension of the Willenborg model that accounts for the

delayed retardation caused by a tensile overload followed by a compressive overload. (Machniewicz 2013)

Apart from crack growth prediction models presented above, several others have been proposed in the literature. Beden *et al.* (2009) published a more detailed review of fatigue crack growth models for constant (CA) and variable amplitude (VA) loading.

2.6 Crack closure

The concept of crack closure was introduced by Elber (1970), who reported that the faces of fatigue crack can come into contact under a remotely applied tensile stress. In other words, the tip of a growing fatigue crack can be closed while the external applied load is still positive. These observations were primarily explained by the residual plastic deformation left in the wake of a fatigue crack tip. The phenomenon is known in the literature as *plasticity induced crack closure* or *Elber mechanism*.

Based on these observations, Elber (1971) stated that crack propagation only occurs during the portion of the loading cycle in which crack tip is fully open. According to this approach, the fatigue crack growth rate da/dN depends on an effective stress intensity factor range ΔK_{eff} :

$$\frac{da}{dN} = f(\Delta K_{eff}) \quad (2.23)$$

which is defined as:

$$\Delta K_{eff} = K_{max} - K_{op} \quad (2.24)$$

where K_{op} is the stress intensity factor associated with a nominal stress necessary to fully open the crack. The concept of effective stress intensity factor range is illustrated in Figure 2.8.

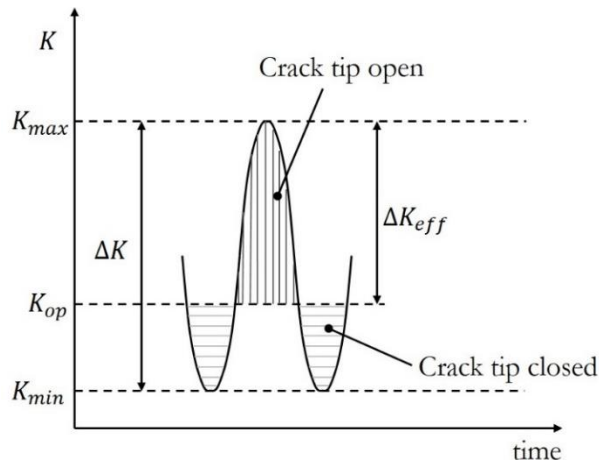


Figure 2.8 – Definition of applied and effective stress intensity factor ranges

This researcher also introduced an effective stress intensity ratio given by the Equation (2.25):

$$U = \frac{\Delta K_{eff}}{\Delta K} = \frac{K_{max} - K_{op}}{K_{max} - K_{min}} \quad (2.25)$$

This parameter indicates the percentage of the loading cycle that contributes to propagate the crack.

Given the previous definitions, Elber further suggested that the crack growth rate can be expressed as a modified Paris relation:

$$\frac{da}{dN} = C(\Delta K_{eff})^n = C(U\Delta K)^n \quad (2.26)$$

Based on experiments carried out on the Al-alloy 2024-T3 over a range of R from -0.1 to 0.7 , Elber determined the following relationship between U and R :

$$U = 0.5 + 0.4R \quad (2.27)$$

Substituting Equations (2.24) and (2.27) in (2.25) leads to:

$$\frac{K_{op}}{K_{max}} = 0.5 + 0.1R + 0.4R^2 \quad (2.28)$$

The Equation (2.28) becomes inaccurate for large negative R -values and may not be extrapolated outside the previously referred range.

Schijve (1981) suggested an improved and more realistic expression valid for $-1 \leq R \leq 1$:

$$U = 0.55 + 0.33R + 0.12R^2 \quad (2.29)$$

which leads to:

$$\frac{K_{op}}{K_{max}} = 0.45 + 0.22R + 0.21R^2 + 0.12R^3 \quad (2.30)$$

ASTM (1999) proposed an additional relation that considers only the positive part of the loading cycle:

$$\begin{aligned} \Delta K &= K_{max} & \text{for } R < 0 \\ \Delta K &= K_{max} - K_{min} & \text{for } R \geq 0 \end{aligned} \quad (2.31)$$

$$U = 0.576 + 0.015R + 0.409R^2 \quad (2.32)$$

which leads to:

$$\frac{K_{op}}{K_{max}} = 0.424 + 0.561R - 0.394R^2 + 0.409R^3 \quad (2.33)$$

Newman (1984) proposed the following expressions for the normalized crack opening stress:

$$\begin{aligned} \frac{\sigma_{op}}{\sigma_{max}} &= A_0 + A_1R + A_2R^2 + A_3R^3 \quad \text{for } R \geq 0 \\ \frac{\sigma_{op}}{\sigma_{max}} &= A_0 + A_1R \quad \text{for } -1 \leq R < 0 \end{aligned} \quad (2.34)$$

when $\sigma_{op} \geq \sigma_{min}$. The coefficients are given by:

$$A_0 = (0.825 - 0.34\alpha + 0.05\alpha^2) \left[\cos \frac{\pi\sigma_{max}}{2\sigma_0} \right]^{1/\alpha} \quad (2.35)$$

$$A_1 = (0.415 - 0.071\alpha) \frac{\sigma_{max}}{\sigma_0} \quad (2.36)$$

$$A_2 = 1 - A_0 - A_1 - A_3 \quad (2.37)$$

$$A_3 = 2A_0 + A_1 - 1 \quad (2.38)$$

where σ_0 is the flow stress given by the average between the uniaxial yield stress and uniaxial ultimate tensile strength of the material and α is the constraint factor. Plane stress or plane strain conditions can be simulated using $\alpha = 1$ or $\alpha = 3$, respectively.

Vormwald and Seeger (1991) and Savaidis *et al.* (1995) developed alternative models based on the plasticity-induced crack closure model proposed by Newman.

Hudak and Davidson (1988) suggested the following model, supported by experimental results:

$$\begin{aligned} U &= \gamma \left(1 - \frac{K_o}{K_{max}} \right) \quad \text{for } K_{max} \leq K_L \\ U &= 1 \quad \text{for } K_{max} \geq K_L \end{aligned} \quad (2.39)$$

where K_o is a constant associated with the threshold for pure mode I fatigue crack growth, K_L is the limiting K_{max} value above which no detectable closure occurs and γ is a parameter defined as:

$$\gamma = \frac{1}{\left(1 - \frac{K_o}{K_L} \right)} \quad (2.40)$$

Ellyin (1997) developed a modified version of the approach proposed by Hudak and Davidson to obtain the effective stress intensity range ΔK_{eff} with constant amplitude loading:

$$\Delta K_{eff,0} = (\Delta K^2 - \Delta K_{th}^2)^{1/2} \approx \Delta K \left[1 - \frac{1}{2} \left(\frac{\Delta K_{th}}{\Delta K} \right)^2 \right] > \Delta K - \Delta K_{th} \quad \text{for } R \approx 0$$

$$\Delta K_{eff} = \frac{\Delta K_{eff,0}}{\left[1 - \left(\frac{\sigma_m}{\sigma_f} \right) \right]} = \frac{\Delta K_{eff,0}}{\left[1 - \frac{(1+R)\sigma_{max}}{2\sigma_f} \right]} \quad \text{for } R \neq 0 \quad (2.41)$$

Recently, Correia, De Jesus, Moreira, and Tavares (2016) proposed an approach based on the same initial assumptions as the two models previously mentioned:

$$U = \left(1 - \frac{\Delta K_{th,0}}{K_{max}} \right) (1 - R)^{\gamma-1} \quad \text{for } K_{max} \leq K_L$$

$$U = 1 \quad \text{for } K_{max} \geq K_L \quad (2.42)$$

where $\Delta K_{th,0}$ is the threshold value of stress intensity factor range for $R = 0$

In addition, Correia, Blasón, *et al.* (2016) proposed the effective stress intensity factor range, ΔK_{eff} , as a replacement for the applied stress intensity factor range, ΔK , in the original CCS fatigue crack growth model:

$$\frac{\log \Delta K_{eff}^* - \log \Delta K_{th,eff}^*}{\log \Delta K_{up,eff}^* - \log \Delta K_{th,eff}^*} = \exp \left[- \exp \left(\frac{\alpha - \log \frac{da^*}{dN^*}}{\gamma} \right) \right] \quad (2.43)$$

The new normalized variables are defined by the expressions (2.44):

$$\Delta K_{eff}^* = \frac{\Delta K_{eff}}{K_c}$$

$$\Delta K_{th,eff}^* = \frac{\Delta K_{th,eff}}{K_c} \quad (2.44)$$

$$\Delta K_{up,eff}^* = \frac{\Delta K_{up,eff}}{K_c}$$

Unlike the original CCS law, the latter model takes into account the plasticity-induced crack closure effects.

Other proposals include numerical approaches based on elastoplastic analysis. Nakagaki and Atluri (1979) proposed an elastoplastic finite element procedure to estimate crack closure and opening effects. McClung and Sehitoglu (1989) presented a model for elastic-plastic finite element simulation of crack growth and crack closure.

2.7 Pre-strain effects on fatigue behaviour

The relevant influence of plastic pre-strain on the subsequent fatigue performance has already been proved by several research workers (Tai Shan and Liu 1974, Sherman 1975, Radhakrishnan and Baburamani 1976, Schijve 1976) Thus, it is important the consideration of the effect of plastic pre-strain on fatigue design procedures.

Many studies on the effect of pre-strain on fatigue behavior of metals have been carried out by several authors with varying experimental techniques and results. Depending on the material properties, the effect of pre-strain on cyclic life may vary from no significant impact to either beneficial or detrimental. In some materials, the pre-straining effect on cyclic behavior differs depending on the ΔK region or strain amplitude considered. Furthermore, it has been proved that the effect of pre-strains on fatigue behavior depends on whether the nature of the pre-strain is tensile or compressive (Sherman 1975).

Therefore, all the results mentioned bellow should be interpreted within the context of each experimental methodology.

2.7.1 Fatigue crack growth tests

Generally, it is reported that low carbon steels exhibit a beneficial increase in fatigue crack growth resistance resulting from prior plastic deformation (Radhakrishnan and Baburamani 1976, Arora *et al.* 1988, Petinov and Melnikov 2011, Donato and Cavalcante 2015). Various authors related this phenomenon with the strain hardening effect, which increases the resistance to flow and reduces the dislocation mobility thus affecting the micromechanisms of fatigue crack growth (Petinov and Melnikov 2011, Donato and Cavalcante 2015, Radhakrishnan and Baburamani 1976). Arora *et al.* (1988) argued that the decrease in fatigue crack growth rates was caused by the compressive surface residual stresses introduced by pre-straining.

Petinov and Melnikov (2011) performed fatigue crack growth tests in the Paris region for a low carbon steel with various levels of plastic deformation ($\delta = 0\%, 1\%, 5\%, 10\%, 15\%$). The experimental results indicated that varying the degree of pre-strain practically does not change the slope of the $da/dN - \Delta K$ curves. Therefore, for the material studied, the Paris exponent is constant and the Paris coefficient (C_δ) is a function of the level of pre-strain (δ), as follows in Equation (2.45):

$$\frac{da}{dN} = C_\delta (\Delta K)^{2.88} \quad (2.45)$$

where C_δ is defined as:

$$C_\delta = 2.504 \cdot 10^{-13} (1 - \delta)^{4.08} \quad (2.46)$$

From the Equation (2.46), it is seen that an increase in the level of plastic deformation leads to a decrease of the Paris coefficient. These results are consistent with the ones previously obtained by Radhakrishnan and Baburamani (1976). Based on fatigue crack growth data from his experiments, Donato and Cavalcante (2015) plotted the Paris coefficient and exponent as a function of the pre-strain level. The plots showed that with increasing degrees of pre-strain, the coefficient increased and the exponent decreased.

Troshchenko *et al.* (1988), in a research conducted on a heat-resistant steel, stated that an increase in the level of pre-straining caused both a decrease in the fatigue crack growth rate in the near threshold region and an increase in the crack growth threshold. The effect in the Paris region was considered to be insignificant. A study conducted by Wang *et al.* (2009) on Q235 low carbon steel led to conclusions contrary to those previously mentioned. It was found that with increasing tensile pre-strain degree, the fatigue crack propagation rate increases considerably in the near threshold region and the fatigue crack growth threshold is reduced.

In studies on aluminum alloys, Schijve (1976) reported a significant increase in fatigue crack growth rate on 2024-T3 alloy after 3% tensile pre-strain while Al-Rubaie *et al.* (2006) observed that the pre-strain level had no significant influence in regions I and II but led to a decrease in fracture toughness of the 7475-T7351 aluminum alloy, thus changing the fatigue crack growth behavior in region III. The latter phenomenon was explained by the strain hardening effect, which increased the strength of material. Schijve (1976) suggested that the higher yield strength, resulting from pre-strain, implied smaller plastic zone sizes, which reduce crack closure and raise the tensile stresses in the plastic zone ahead of the crack thus enhancing crack growth. Tai Shan and Liu (1974) also observed an accelerated crack growth in 2024-T351 aluminum alloy.

Wu *et al.* (2013) performed fatigue tests on SUH660 stainless steel to understand the effect of pre-strain on fatigue strength. These tests revealed a crack growth acceleration for the pre-strained specimens, which is thought to be related to precipitate cutting. Wu proposed a dislocation accumulation model for the fatigue crack tip in the precipitation-strengthened material. In a research conducted by Vor *et al.* (2009) on 304L stainless steel, the previously deformed material exhibited a decrease in the crack propagation threshold when compared to the as-supplied material.

Jiang and Chen (2012) studied the X60 pipeline steel and concluded that pre-tension deformation accelerates fatigue crack propagation and decreases the fatigue crack propagation threshold ΔK_{th} .

Contrary to these findings, an investigation carried out by Netto *et al.* (2008) in the API 5L X60 pipeline steel showed that prior plastic deformation decelerates fatigue crack propagation. Moreover, it was verified that, for the pre-strained specimens, the Paris coefficient is higher and the Paris exponent is lower as compared to the non-pre-strained specimens.

A study on dual-phase (DP) steels, conducted by Nakajima *et al.* (1999), revealed that the propagation of a fatigue crack in the pre-strained material was faster when compared to the non-pre-strained one. Furthermore, fatigue crack growth threshold was substantially reduced in the pre-strained material. These conclusions are in agreement with the work of Godefroid *et al.* (2005).

Among the various experimental procedures of the aforementioned works, it is important to highlight the one adopted by Donato and Cavalcante (2015). In his work, strips were cut from the original steel plate and subsequently subjected to a uniaxial stress, which generated a previously selected pre-strain level. After that, CT specimens were extracted from the strips. This pre-straining procedure led to highly uniform strain distributions along the strips, which minimized residual stress and crack closure plasticity-induced effects. However, in most of the studies in the literature, the application of pre-strain is performed in machined CT specimens. This method imposes a residual stress gradient which may change the fatigue crack growth behavior.

2.7.2 Stress life approach

Studies in low carbon steels have shown a trend for increased fatigue limits with increasing pre-strain levels (Radhakrishnan and Baburamani 1975, Kage and Nisitani 1977, Libertiny *et al.* 1977, Gustavsson and Melander 1995, Uemura 1998). Radhakrishnan and Baburamani (1976) stated that the pre-straining operation induced compressive surface residual stresses in the material, which were responsible for an increase in the nucleation period of the fatigue crack. In addition, it was shown that the increase in fatigue strength resulting from the pre-strain is directly proportional to the degree of pre-strain. Other investigations found that small pre-strains often lead to a decrease in the fatigue limit of low carbon steels (Nagase and Kanri 1992,

Nagase and Suzuki 1992). Furthermore, Libertiny *et al.* (1977) concluded that a very large pre-strain ($0.95\varepsilon_f$) caused a drop in the fatigue strength to its initial value.

On the other hand, a detrimental effect on the fatigue limit has been observed for quenched and tempered 0.45% C steel, which is thought to be related to cyclic softening (Nisitani *et al.* 1987). The same behavior was verified in fatigue tests performed on a quenched and tempered 0.5% C steel. In this case, the author argued that the decrease in the fatigue limit was due to the generation of pre-cracks on the surface of the material as a result of pre-strain (Kang *et al.* 2007).

For the 7050-T7451 aluminum alloy, Al-Rubaie *et al.* (2007) concluded that the tensile pre-strain triggered a decrease in the number of cycles to failure as compared to the as-received material under the same $\Delta\sigma$.

An investigation carried out by Zheng *et al.* (2005) in X60 pipeline steel revealed that pre-deformation caused strain hardening of the material, thus increasing its fatigue limit.

Chiou and Yang (2012) investigated the fatigue behavior of SUS 430 Stainless Steel subjected to tensile plastic pre-deformation. In comparison with the as-received material, a reduction in fatigue life has been observed under the same loading condition and the extent of that reduction increased with increasing level of pre-strain. This deterioration in fatigue performance was attributed to the early occurrence of fatigue crack initiation and propagation generated by the applied tensile pre-strain.

In opposition, fatigue tests conducted by Libertiny *et al.* (1977) in HSLA steels showed significant improvement in fatigue strength for the pre-strained material.

Lanning *et al.* (2002) stated that the effect of pre-straining on the fatigue limit may be a function of several variables, namely material, strain rate during pre-straining, nature of pre-strain, re-machining of the pre-strained specimens before cycling, type of fatigue loading, stress ratio, frequency, control mode during cycling and number of cycles to failure.

2.7.3 Strain life approach

Ganesh Sundara Raman and Padmanabhan (1996) conducted strain-controlled fatigue tests to understand the effect of prior cold work on the low-cycle fatigue behavior of the AISI 304LN stainless steel. The experimental results displayed that plastic deformation improved high-cycle fatigue life and decreased low-cycle fatigue life. Based on previous studies, it can be said that fatigue life is controlled by the ductility of a material at high strain amplitudes, which are mostly

plastic and by its strength at low strain amplitudes, which are mostly elastic (Suresh 1998, Campbell 2008). Therefore, the detrimental effect on low cycle fatigue life (LCF) was explained by the reduced ductility of the pre-strained material. For low strain amplitudes, the increase in fatigue life was related with the increase in strength produced by pre-straining. In other words, for the same strain amplitude, the more pre-strained material exhibited higher cyclic stresses comparing to the less pre-strained material. In terms of LCF parameters, cold working decreased the fatigue ductility coefficient and exponent (ϵ'_f and c) and increased the fatigue strength coefficient and exponent (σ'_f and b) (Ganesh Sundara Raman and Padmanabhan 1996).

Similar results had already been obtained by Sherman (1975) for HSLA steels specimens pre-strained in compression. Moreover, Sherman also found that a 14% tensile pre-strain in a hot-rolled low-carbon steel increased fatigue life at high and low strain amplitudes and decreased it at intermediate amplitudes. Some HSLA steels differ from the above descriptions and display a reduced fatigue life at small strain amplitude, which is associated the fact that the increase in strength produced by pre-straining is lost under cyclic loading.

In another research work, Sherman and Davies (1979) observed an approximately 50% increase in the fatigue life of a DP steel, at all strain amplitudes. This favorable behavior is made possible by the high ductility of the material. Similar results were reported by Fredriksson *et al.* (1988) after testing DP steels.

The experiments performed by Robertson *et al.* (2008) in a TRIP 780 multiphase steel demonstrated that cold rolling increases considerably the number of reversals to failure at high strain amplitudes, while having little or no effect in the high cycle region. Ly and Findley (2016) argued that LCF lives are relatively independent of the pre-strain history. Nevertheless, a significant decrease in LCF life was verified for the 15% pre-strain condition.

Another relevant investigation was carried out by Singh (2010), in which it was concluded that cold rolling led to significant increase in LCF life of various materials at low strain amplitudes.

3 Experimental procedure

3.1 Description of the material

In this study, fatigue crack propagation tests were performed using specimens produced with the P355NL1 steel. The P355NL steel is a fine grain low alloy carbon steel suitable for pressure vessel purposes with special requirements for low temperatures. P355NL1 is the designation of the steel, according to the EN10028-3 standard. Table 3.1 lists the corresponding designations for other standards.

Table 3.1 - Designation of the P355NL1 steel according to different standards

Europe	Italy	China	Germany	France	UK	Russia	USA
EN	UNI	GB	DIN	AFNOR	B.S.	GOST	AISI/SAE
P355NL1	FeE 355 KT	Q420q- D	TStE355	A510 FP	255- 490~	22K	LF6

The chemical composition and mechanical properties of the pressure vessel steel, obtained from De Jesus *et al.* (2006), are given in Table 3.2 and Table 3.3, respectively.

Table 3.2 - Chemical composition in wt% of P355NL1 steel, from (De Jesus, Ribeiro, and Fernandes 2006)

C	Si	Mn	P	S	Al	Mo
0.144	0.199	1.443	0.018	0.006	0.034	0.001
Nb	Ni	Ti	V	Cu	Cr	
0.009	0.037	0.001	0.003	0.059	0.031	

Table 3.3 - Mechanical properties of the P355NL1 steel (De Jesus, Ribeiro, and Fernandes 2006)

σ_{UTS} (MPa)	σ_y (MPa)	ϵ_r (%)	E (GPa)	ν
568	418	30	205.2	0.275

Table 3.4 displays the values of the Morrow constants for the P355NL1 steel, as determined by De Jesus and Correia (2013).

Table 3.4 - Morrow constants of the P355NL1 steel (De Jesus and Correia 2013)

σ'_f (MPa)	b	ϵ'_f	c
1005.5	-0.1033	0.3678	-0.5475

The monotonic and cyclic elastoplastic properties of the material are summarized in Table 3.5, according to De Jesus and Correia (2013).

Table 3.5 - Monotonic and cyclic elastoplastic properties of the P355NL1 steel (De Jesus and Correia 2013)

K (MPa)	n	K' (MPa)	n'
611.49	0.063	948.35	0.1533

De Jesus *et al.* (2007) also conducted fatigue crack growth experiments on the P355NL1 steel. The crack propagation rates for distinct stress ratios ($R = 0$, $R = 0.5$ and $R = 0.7$) were investigated using CT specimens. Figure 3.1 displays the experimental results in the form of $da/dN - \Delta K$ curves. The Paris law constants (C and m), obtained for each stress ratio, are presented in Table 3.6.

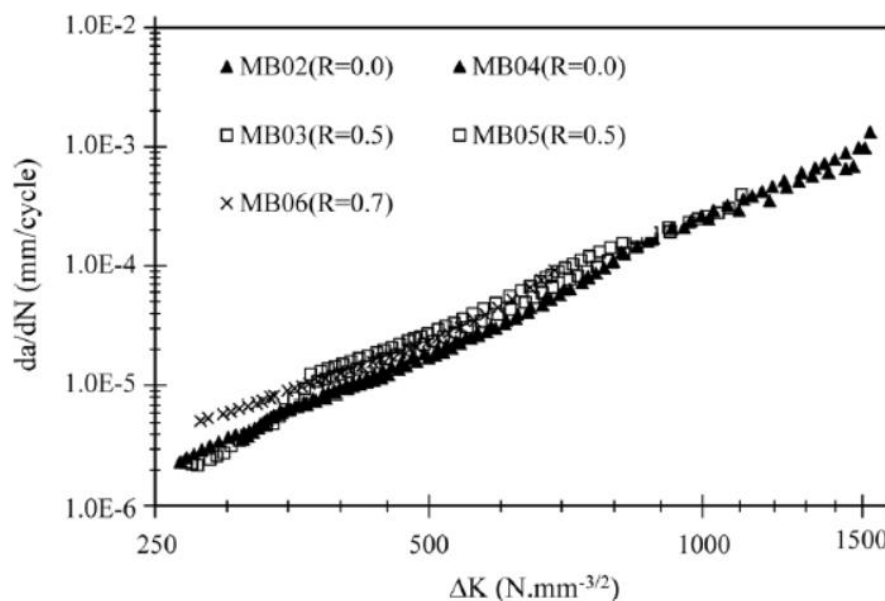


Figure 3.1 - Fatigue crack propagation data of P355NL1 steel for different stress ratios (De Jesus *et al.* 2007)

Table 3.6 - Paris law constants for different stress ratios (De Jesus *et al.* 2007)

Parameter	$R = 0$	$R = 0.5$	$R = 0.7$
C	7.195×10^{-15}	6.281×10^{-15}	2.037×10^{-13}
m	3.499	3.555	3.003

From these results, De Jesus *et al.* (2007) concluded that crack propagation rates are only slightly influenced by the stress ratio. Nevertheless, it was observed that stress ratio effects are more pronounced for lower values of the stress intensity factor range. For $R = 0$ and $R = 0.5$, the determined Paris' law curves were approximately parallel to each other.

The material was delivered as a 5 mm thick sheet in the normalized condition. The major dimensions of the steel sheet are shown in Figure 3.2.

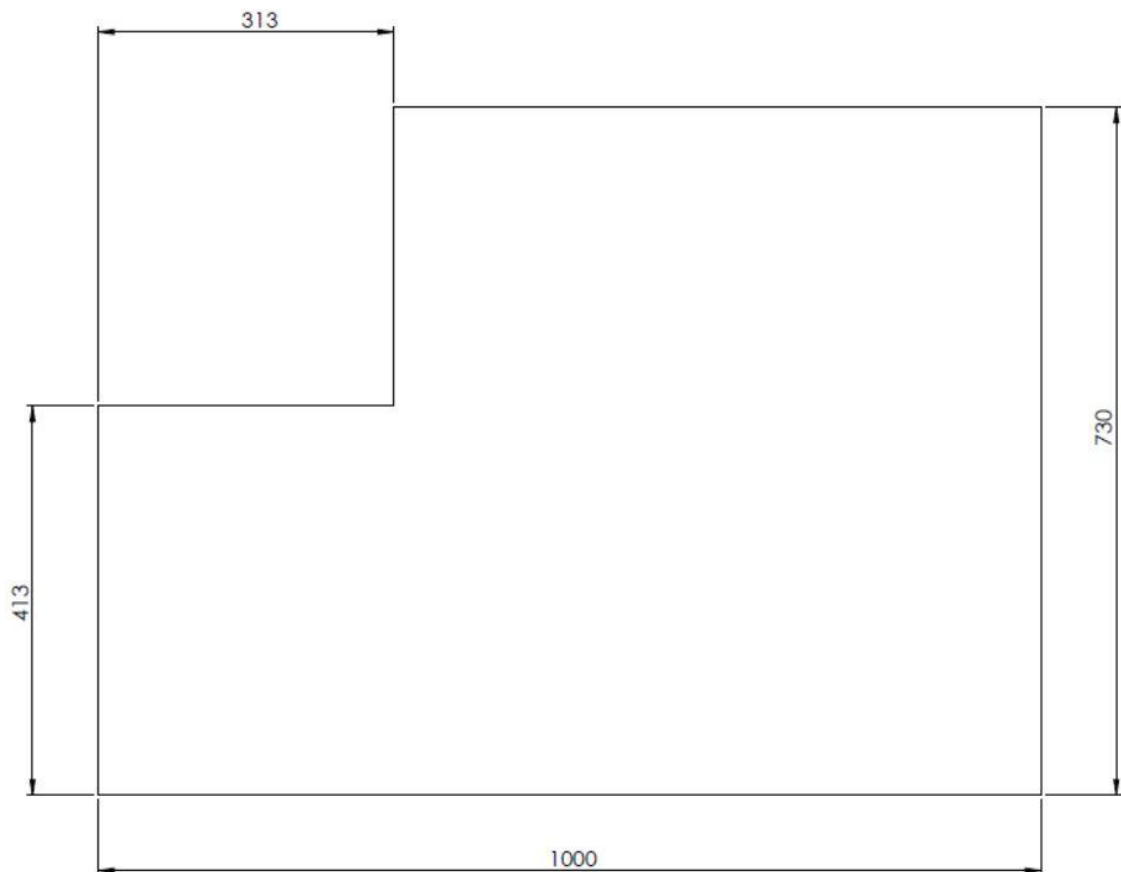


Figure 3.2 - Planar dimensions of original P355NL1 sheet

3.2 Fatigue tests program

A targeted program of testing was conceived in order to investigate the effect of plastic pre-strain in the subsequent fatigue crack growth behavior of the steel.

The fatigue performance was investigated for two types of loading condition:

- Pure mode I loading (Uniaxial)
- Mixed mode (I+II) loading (Multiaxial)

Compact tension (CT) specimens, with three different levels of pre-strain (0%, 3% and 6%), were employed for pure mode I fatigue tests.

For mixed mode (I+II) loading condition, non-strained (0% pre-strain) and 6% tension pre-strained compact tension shear (CTS) specimens were used. The behavior of CTS specimens was analyzed for two loading angles ($\alpha = 30^\circ$ and $\alpha = 60^\circ$) in an attempt to perceive the influence of different mixed mode loadings.

For each pre-strain level and loading angle (α), specimens were tested under two distinct stress ratios, namely $R = 0.05$ and $R = 0.5$.

For a determined pre-strain level and loading condition (R and α), two specimens were used to cover possible data scatter.

The experimental program is summarized in Table 3.7.

Table 3.7 - Experimental program

Specimen type	Type of test	Pre-strain	R_σ	α	No. of specimens	Laboratory
CT	Pure Mode I	0%	0.05	0°	2	Gijón Polytechnic School of Engineering University of Oviedo (Spain)
			0,5		2	
		3%	0.05	0°	2	
			0,5		2	
		6%	0.05	0°	2	
			0,5		2	
CTS	Mixed Mode (I + II)	0%	0.05	30°	2	Wrocław University of Science and Technology (Poland)
			0,5		2	
			0.05	60°	2	
			0,5		2	
		6%	0.05	30°	2	
			0,5		2	
			0.05	60°	2	
			0,5		2	

In total, the experimental program comprises twelve compact tension (CT) specimens and sixteen compact tension shear (CTS) specimens of P355NL1 steel.

3.3 Specimens

3.3.1 Compact tension specimens

The CT specimen was designed in accordance with the recommendations of the ASTM E647 standard (ASTM 2000). For practical reasons, it was imposed that the two holes in the CT specimen had 12 mm in diameter. This measure concurs with a nominal width $W = 48 \text{ mm}$, a height of 57.6 mm and a normalized crack size $a/W = 0.2625$. The initial notch length, measured from the loading holes, was 9.6 mm. Since the base sheet of steel measured approximately 5 mm in thickness, a 4.35 mm thickness was adopted for both CT and CTS specimens.

Figure 3.3 presents the dimensions of the CT specimen according to ASTM E647.

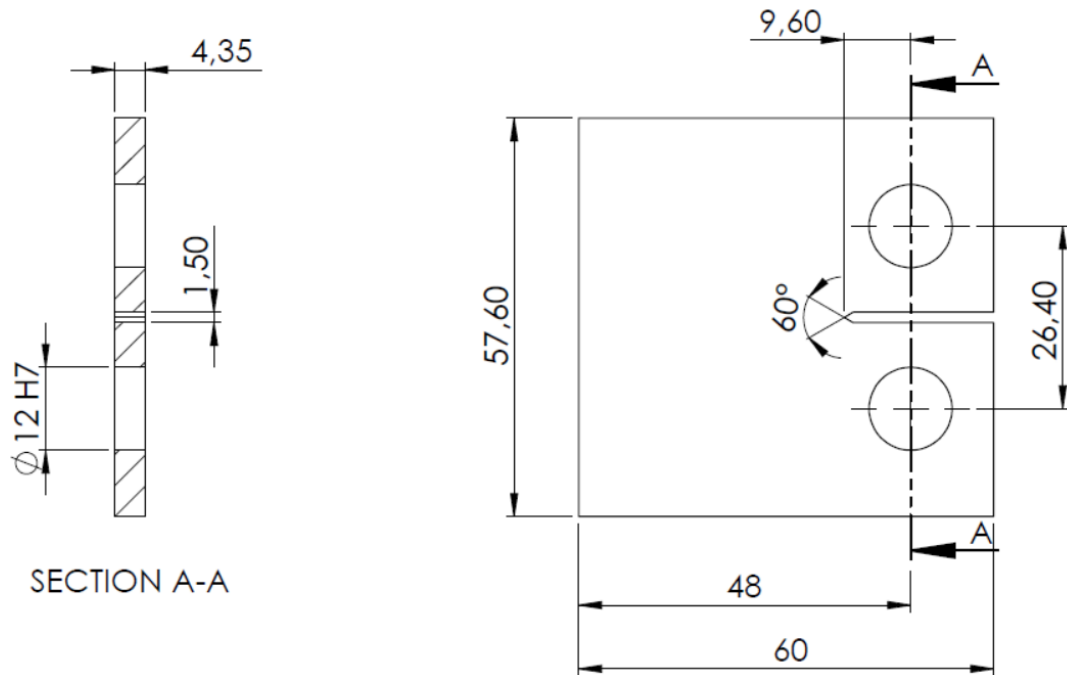


Figure 3.3 – Dimensions (in mm) of the CT specimen used for pure mode I fatigue tests, in accordance with ASTM E647

Besides the geometry established by the ASTM E647 standard, two additional M3-6H holes were needed to later fasten the knife edges, in which the arms of the clip gauge fit. This detail is represented in Figure 3.4.

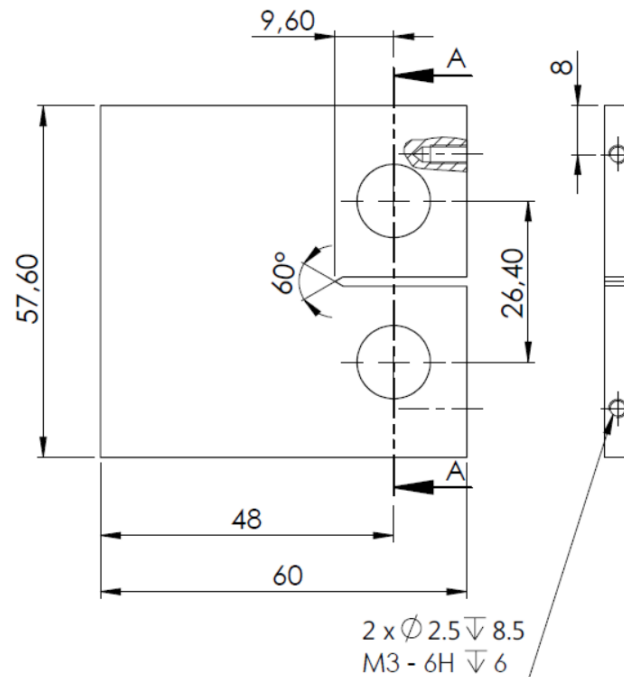


Figure 3.4 - CT specimen with threaded hole

3.3.2 Compact tension shear specimens

The geometry of the CTS specimen for mixed mode fatigue crack growth tests was originally proposed by Richard (1985). The CTS specimens used in this investigation have a width equal to 42 mm and a height equal to 71.4 mm. The total initial notch depth was 16.9 mm. As stated earlier, all specimens are 4.35 mm thick. These dimensions were defined based on the recommendations made by Richard (1985). The specimen's shape and dimensions are shown in Figure 3.5.

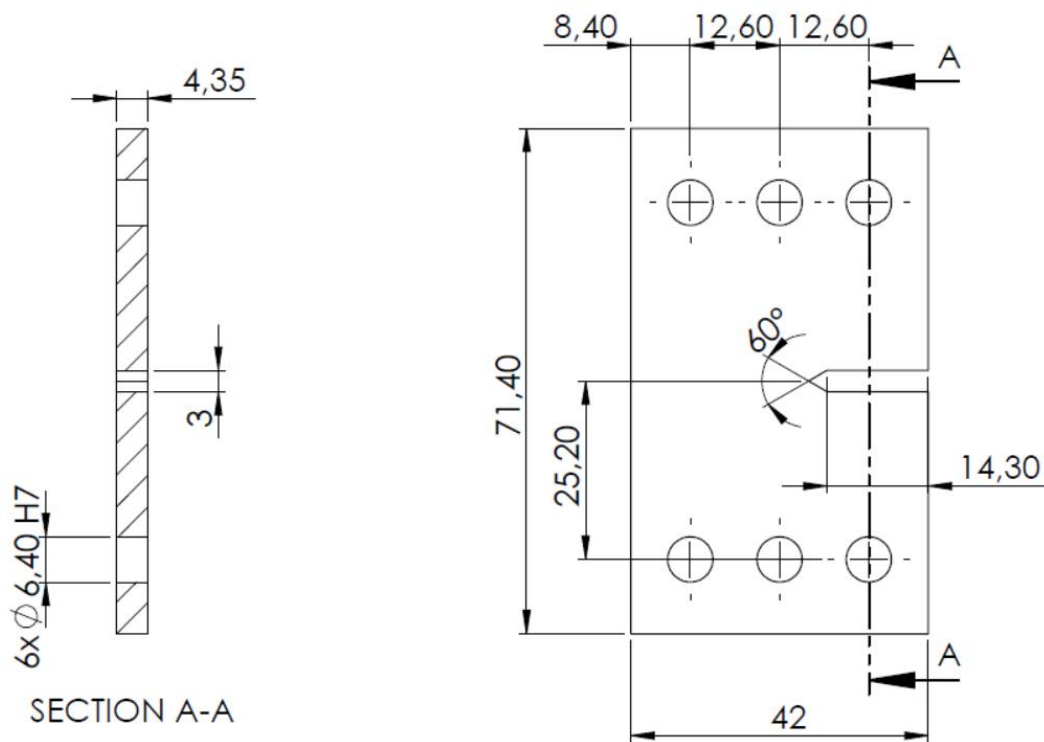


Figure 3.5 – Dimensions (in mm) of the CTS specimen used for mixed mode fatigue tests

3.4 Microstructure of the material in the as-received condition

3.4.1 Metallographic sample

Before planning the extraction of the specimens from the base sheet of P355NL1, it was necessary to determine the rolling direction. Therefore, a small sample was cut in the longitudinal direction of the original steel sheet in order to carry out a microstructural analysis. Figure 3.6 shows the orientation of the metallographic sample relative to the P355NL1 sheet. The directions L, LT and ST stand for longitudinal, long transverse and short transverse (thickness), respectively. The microstructure was observed in three different surfaces, defined by the planes L-LT, L-ST and LT-ST, to clearly identify the rolling direction. The following metallographic procedures for sample preparation were performed on all these surfaces.

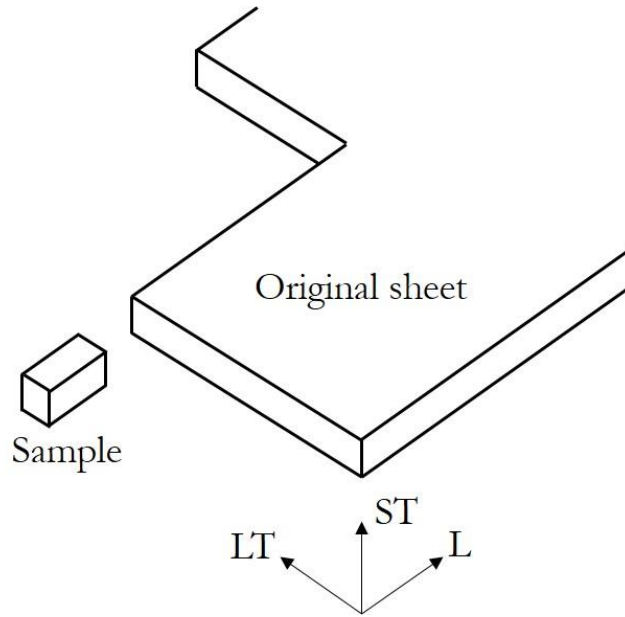


Figure 3.6 - Sample's position relative to the original sheet

3.4.2 Metallographic sample preparation

Firstly, the specimen's surfaces were grinded through a series of four SiC abrasive papers with increasingly finer grit sizes (80, 180, 320 and 800). The grinding was performed on a Struers Rotopol-21 machine with two rotating platens at 300 rpm and tap water lubrication (Figure 3.7).

Afterwards, the sample was polished with an alumina suspension on a rotating felt disc. This step was executed on a Struers DP-U4 machine (Figure 3.8). Thereafter, the surfaces were sequentially polished with 3 and 1-micron diamond spray on a rotating felt polishing cloth. The diamond polishing operation was carried out on a Struers Rotopol-21 machine with blue lubricant (Figure 3.9). All polishing steps were performed with rotating discs at 150 rpm.

Between each polishing and grinding step, the specimen was rotated 90° and thoroughly cleaned with water to make sure previous grade abrasives were removed.



Figure 3.7 - Struers RotoPol-21 equipped with 320 and 800 grit SiC abrasive papers and tap water lubrication



Figure 3.8 - Struers DP-U4 with felt disc and alumina suspension



Figure 3.9 - Struers RotoPol-21 with diamond spray

After polishing, the sample was cleaned with 96% alcohol, dried with cotton and etched in 2% Nital to optically enhance microstructural features. Prior to the observation, the sample was cleaned with water, to remove the remaining Nital, and then dried.

3.4.3 Microstructural observation

The sample's surfaces were analyzed in a Zeiss Axiophot optical microscope with a resolution up to 100x. The microscope (Figure 3.10) is connected to a computer that possesses a software capable of reproducing and saving the image seen through the objective for further analysis.



Figure 3.10 - Zeiss Axiophot optical microscope

3.4.4 Results

Figure 3.11, Figure 3.12 and Figure 3.13 show the optical micrographs of three different surfaces of the metallographic specimen.



Figure 3.11 - Microstructure for the P355NL1 steel in the L-LT direction

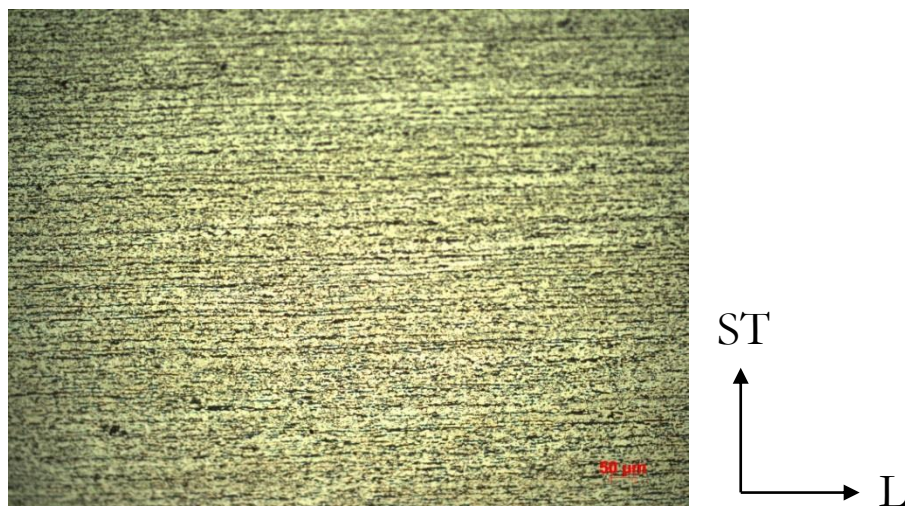


Figure 3.12 - Microstructure for the P355NL1 steel in the L-ST direction

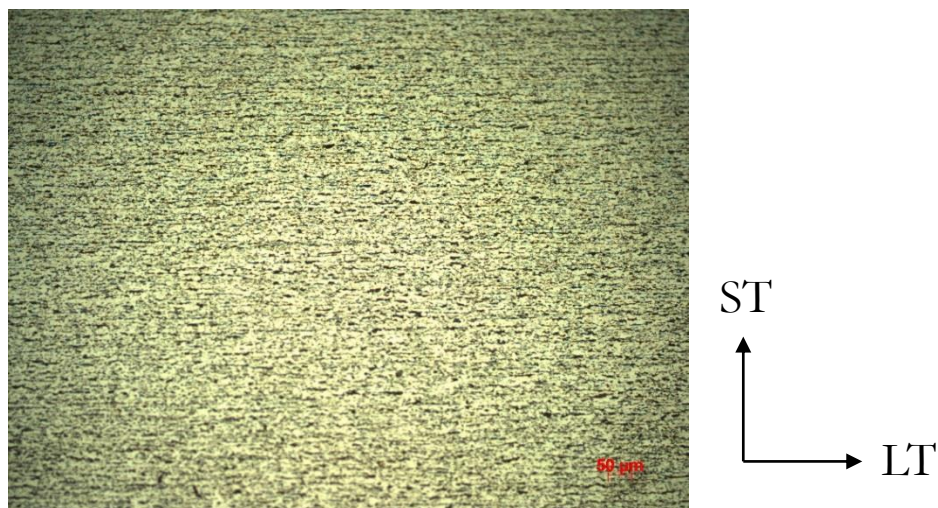


Figure 3.13 - Microstructure for the P355NL1 steel in the LT-ST direction

3.4.5 Interpretation of the rolling direction

Figure 3.11, Figure 3.12 and Figure 3.13 present the typical ferrite perlite grained-refined microstructure of the P355NL1 steel. From Figure 3.11, it is not clear the preferential orientation of the grains, introduced by the rolling process. However, in Figure 3.12, it is possible to observe a banded microstructure with elongated grains aligned in the L direction. Figure 3.13 also shows some strained grains in the LT direction, although not so pronounced as in the previous case. Thus, it can be concluded that the rolling direction is the L direction.

3.5 Specimens orientation

Having determined the rolling direction, it is possible to define the orientation of the CT and CTS specimens relative to the original sheet.

Since the P355NL1 is a steel intended for pressure service applications, it becomes relevant to consider the case of a pressure vessel.

Considering a thin walled pressurized vessel, with a diameter d and thickness t , under an internal pressure p , the maximum stress applied to the vessel is the tangential stress σ_t :

$$\sigma_t = \frac{pd}{2t} \quad (3.1)$$

Therefore, it is expected that the first cracks will appear in the longitudinal direction, i.e. in the direction normal to the maximum stress. On the other hand, the manufacturing process of a pressure vessel introduces a plastic strain in the circumferential direction of this structure (straining direction). This scenario is illustrated in Figure 3.14. From these considerations, one can conclude that the direction of pre-strain application should be normal to the direction in which the initial crack will later be introduced in the specimen (Figure 3.15).

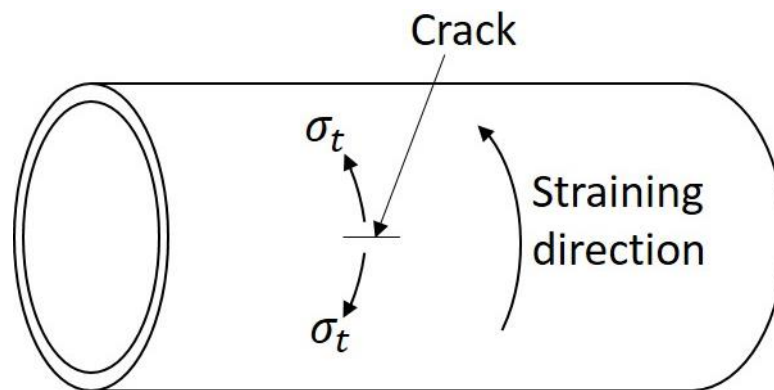


Figure 3.14 - Illustration of a crack in a pressure vessel

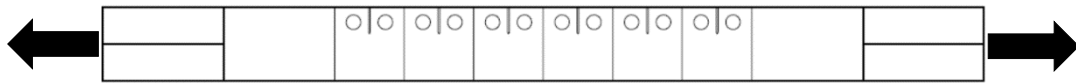


Figure 3.15 - Orientation of the specimens relative to the pre-straining direction

Moreover, previous investigations (Hertzberg and Nordberg 1969, Shimizu and Torii 2002, Moreira *et al.* 2008) have revealed that cracks propagate faster in the direction of rolling compared to the transverse directions, due to elongated grains in the former direction.

Therefore, the CT specimens should be machined in such a way that the pure mode I loading axis is oriented parallel to the rolling direction (or LT direction) and the initial crack is aligned in the L direction. The orientation of the specimens relatively to the original sheet is illustrated in Figure 3.16.

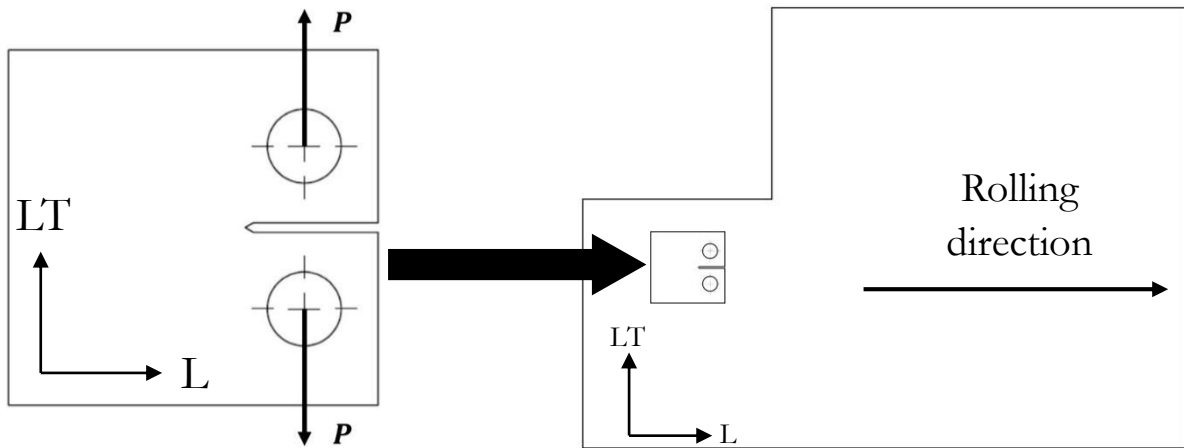


Figure 3.16 - Orientation of the CT specimen relative to the original sheet of P355NL1

Analogously, the CTS specimens will be machined with the same orientation as the CT specimens.

3.6 Sheet's planning

After the number of specimens and their orientation had been defined, a plan was drawn up for the extraction of the various specimens from the delivered P355NL1 sheet. The planning for the original sheet is presented in Figure 3.17.

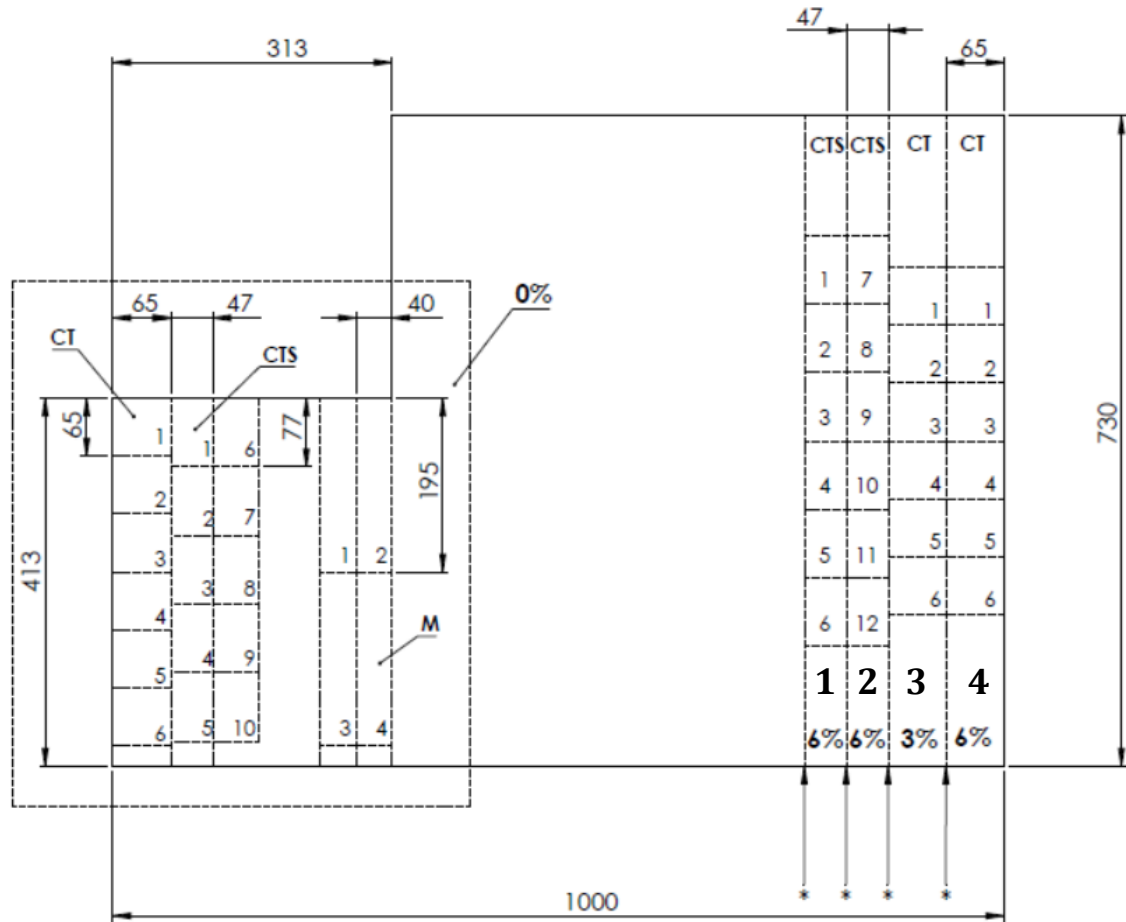


Figure 3.17 - Planning for specimen extraction (dimensions in mm)

In the left part of the planning are the non-pre-strained specimens. Six CT specimens and ten CTS specimens will be directly extracted from steel sheet, without experiencing any intermediate plastic pre-straining operation.

In the right side, the plastically pre-strained specimens are represented. The strips, indicated with ‘*’, will be cut from the original sheet and subsequently subjected to tensile plastic deformation. For organizational purposes, the different strips were identified with a number and corresponding pre-strain level, which are specified in their lower part. Thereafter the specimens will be extracted from the central region of the strips, where the strain field is supposed to be more uniform and closer to the desired value. Six specimens will be extracted from each of the four strips, as numbered in Figure 3.17. The type of specimen to be obtained from each strip is indicated in the upper section of the strip.

It is noteworthy that all specimens have oversized dimensions, due to the fact that they will be cut and subsequently machined to obtain the desired dimensions. Thus, the dimensions presented in the sheet's planning are not the final dimensions of the specimens.

3.7 Application of the pre-strain

As indicated in Figure 3.17, four strips were cut in the LT direction for posterior application of the desired pre-strain levels. Figure 3.18 shows the cut strips, differentiated by the number and pre-strain to be applied.

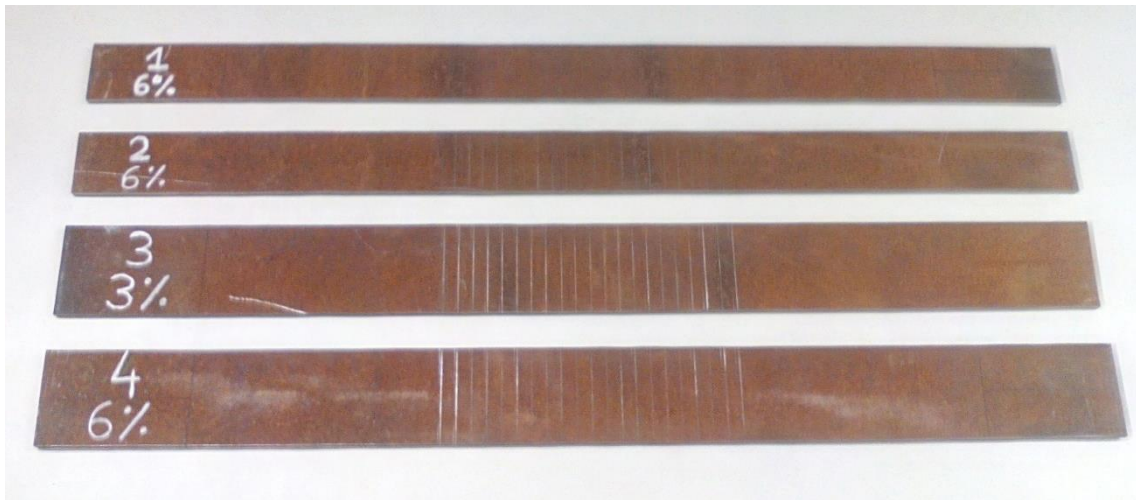


Figure 3.18 - The four steel strips with 10 mm spaced engraved lines

In order to measure the longitudinal strain during the application of the pre-strain, a linear strain gage was mounted on the center of each strip. Additionally, twenty equally spaced (10mm) lines were engraved around the center of the steel strips using a metal scratcher.

3.7.1 Instrumentation of the strips

Preparation of the measurement surface

Prior to the strain gauge installation, it was necessary to carefully prepare the surface around the measuring spot. The objective was to create a smooth surface which can be easily wetted to enable a successful adhesive bonding and avoid incorrect measurements.

Firstly, a grinding machine was used to remove rust and other contaminants from the location where the strain gages would be installed. Subsequently, the surface was grinded using silicon carbide abrasive papers with increasingly finer grits (180, 320 and 800), in order to remove scars, deep scratches and other surface imperfections. Between each grinding step, the direction

of grinding was rotated 90°. After the surface was smooth, the 800 grit silicon carbide paper was rubbed in the diagonal directions to create small scratches and enlarge the contact area between adhesive and strip, thus increasing the bonding forces. Next, a final cleaning was necessary to degrease the gage area and remove any loose particles. The surface around the measuring point was carefully cleaned with cotton and acetone. After these procedures, the gage area was prepared to receive the strain gage.

Installation of the strain gage

The installed strain gages (Figure 3.19), made by Tokyo Sokki Kenkyujo Co., Ltd, had a 6 mm gage length and a resistance of 120 Ω .

To take the strain gage out of the protection foil and facilitate its handling, adhesive tape was placed in the top side of the gage. Subsequently, the adhesive tape with the strain gage was aligned on the steel strip so that the longitudinal direction of the gage was parallel to the longitudinal direction of the strip. After this alignment was achieved, a portion of the tape was bended upwards to provide access to the gage area while maintaining the alignment. Strain gage adhesive was applied to the measurement surface and the adhesive tape was bended down and lightly pressed against the strip. After three minutes, the adhesive tape was removed and the bonding between the steel strip and the strain gage was successfully established.



Figure 3.19 - Strain gage installed on steel strip

After the strain gage was installed, its solder terminals were soldered to lead wires. The solder and soldering iron were firmly applied to the strain gage's solder tabs for one second and then simultaneously removed. Finally, the strain gage resistance was checked using a multimeter

and a protective coating was applied over the entire gage and terminal area. Figure 3.20 shows the four steel strips with strain gages installed and lead wires soldered.

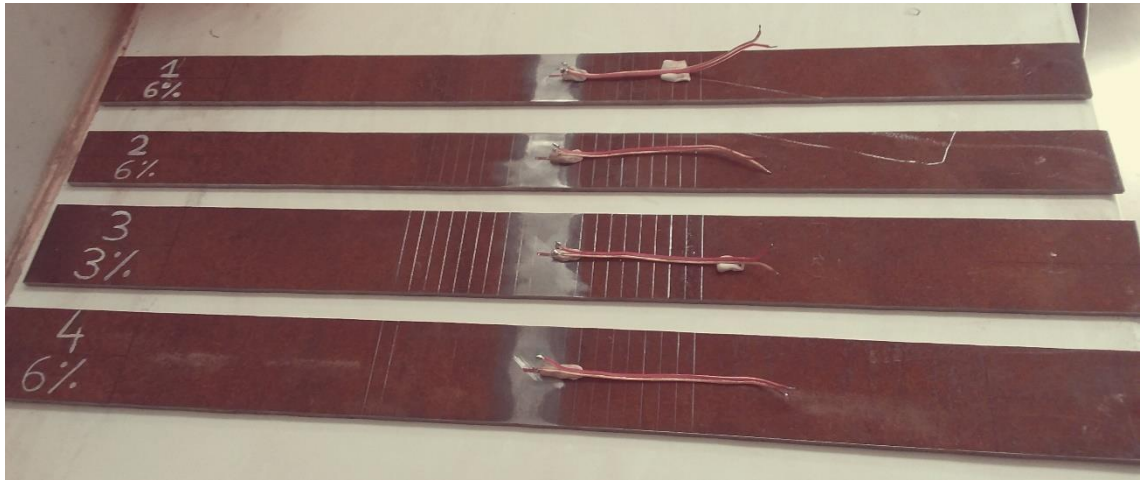


Figure 3.20 - The four steel strips with strain gages installed and lead wires soldered

Data logger

In order to measure and record strain data throughout the pre-straining operation, the strain gage was connected to a data logger. The device used was a data logger model TDS-530 made by Tokyo Sokki Kenkyujo Co., Ltd (Figure 3.21), controlled by a computer. The measured strain and force values were displayed in a color LCD monitor in real time. The data from the tests was stored in a compact flash memory card.

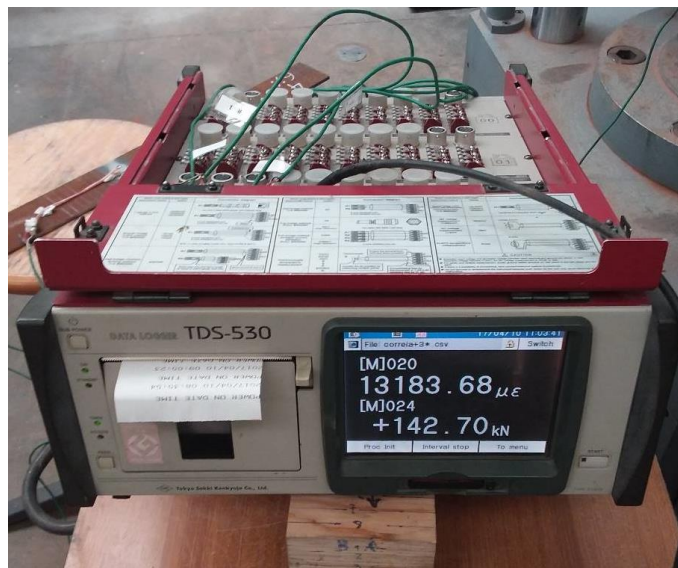


Figure 3.21 - Data logger model TDS-530 made by Tokyo Sokki Kenkyujo Co., Ltd

3.7.2 Pre-straining procedure

The tensile pre-straining of the strips was conducted at the Department of Civil Engineering, Faculty of Sciences and Technology, University of Coimbra in a *Servosis* MUF 404/100 universal testing machine, equipped with a 1000 kN load cell (Figure 3.22). The tests were carried out under displacement control mode. The control was performed by a computer with the *PCD2K* software. Furthermore, the computer displays records and processes the test results.



Figure 3.22 - Servosis MUF 404/100 testing machine

Initially, each end of the strip was secured in the machine's wedge action grips. The strips had marks at 100 mm from the ends to indicate the clamping zones and a midline to assist in their correct alignment, as illustrated in Figure 3.23. White area is the tensile zone and grey areas are the clamping zones. Figure 3.24 shows the steel strip mounted in the testing apparatus.

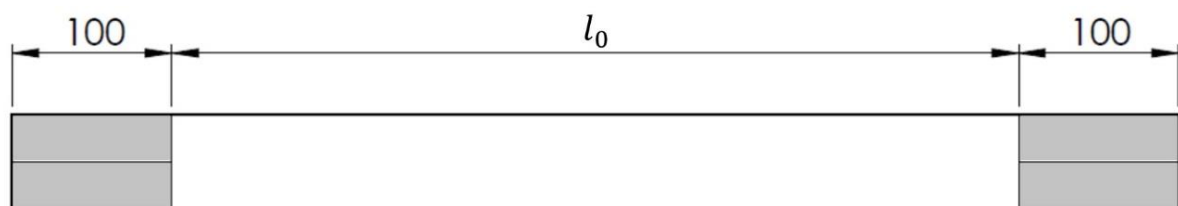


Figure 3.23 - Scheme of the strip's zones



Figure 3.24 - Strip mounted in the Servosis MUF 404/100 universal testing machine

After the strip was clamped, it was necessary to enter the final displacement (in mm) and the displacement rate (in mm/s) in the computer interface. The latter could be altered throughout the test. The theoretical final displacement ($\Delta l_{theoretical}$) was calculated based on the theoretical strain ($\epsilon_{theoretical}$) and on the length of the tensile zone (l_0). The theoretical strain ($\epsilon_{theoretical}$) is the pre-strain which was previously defined for the various strips. It is noteworthy that only the length of the tensile zone (l_0) is used to calculate the theoretical final displacement instead of the entire length of the strip. This is due to the assumption that there is no sliding in the clamping zone and thus strain occurs only in the tensile zone.

For example, in strip number 1:

$$\epsilon_{theoretical} = \frac{\Delta l_{theoretical}}{l_0}$$
$$\Leftrightarrow \Delta l_{theoretical} = \epsilon_{theoretical} \times l_0 = 6\% \times 528 = 31.68 \text{ mm} \quad (3.2)$$

The theoretical strain, tensile length and test parameters (theoretical displacement and displacement rate) are presented in Table 3.8.

Table 3.8 - Test parameters

Strip number	$\varepsilon_{theoretical}$	l_0 [mm]	$\Delta l_{theoretical}$ [mm]	Displacement rate [mm/s]
1	6%	528	31.68	0.01+0.05
2	6%	528	31.68	0.05
3	3%	529	15.87	0.05
4	6%	530	31.80	0.05

To verify if there is any influence of the displacement rate on the real pre-strain applied, a 0.01 mm/s rate was used for strip 1 until the displacement was 20 mm. After that, the displacement rate was changed to 0.05 mm/s. All other strips were pre-strained at a constant 0.05 mm/s displacement rate.

During the test, the test screen displays the force-displacement plot in real time, as may be seen in Figure 3.25. The instantaneous values of force and displacement are also exhibited.



Figure 3.25 - Test screen with load-position plot and instant values of displacement and force

The results of the tensile pre-straining operation obtained from the Servosis universal testing machine are presented in Table 3.9. In other hand, Table 3.10 lists the results indicated by the data logger connected to strain gage. The relative error (in percentage) was calculated as follows:

$$relative\ error\ (\%) = \frac{|\varepsilon_{theoretical} - \varepsilon_{real}|}{\varepsilon_{theoretical}} \quad (3.3)$$

Table 3.9 - Results from the Servosis universal testing machine

Strip number	Tensile length [mm]	Δl_{real} [mm]	Maximum load [kN]	ϵ_{real} [%]	Relative Error [%]
1	528	30.86	117.47	5.84	2.7
2	528	29.98	117.77	5.68	5.3
3	529	15.98	149.67	3.02	0.7
4	530	30.00	169.45	5.66	5.7

Table 3.10 - Results from the data logger connected to strain gage

Strip number	Maximum load [kN]	Maximum strain [%]	Relative Error [%]
1	116.60	4.46	25.7
2	115.82	3.95	34.2
3	148.27	1.61	46.3
4	167.68	2.10	65.0

The high relative error in the strain registered by the data logger can be explained by a possible displacement or sliding of the strain gage, which cause the measured values to be lower than the real values.

The dimensions of the strips before and after the pre-straining were measured. The width and thickness were measured in three different sections (Figure 3.26) with a Vernier caliper. The length of the tensile zone (l_0) was measured using a tape measure. Therefore, the resolution of the measurement is different.

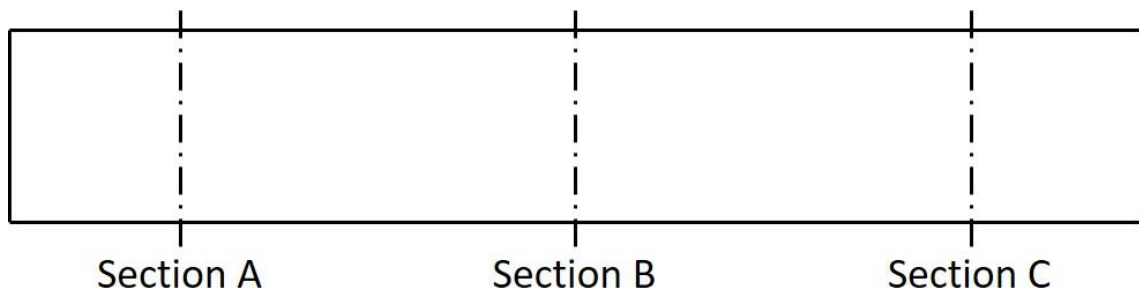


Figure 3.26 – Strips' sections

Table 3.11 and Table 3.12 present the dimensions of the four strips before and after the pre-straining, respectively.

Table 3.11 – Strips' dimensions before pre-straining

Strip number	l_0 [mm]	Width [mm]			Thickness [mm]		
		Section A	Section B	Section C	Section A	Section B	Section C
1	528	47.60	47.60	47.60	5.45	5.80	5.35
2	528	48.30	48.45	48.15	5.45	5.80	5.50
3	529	65.45	64.45	64.85	5.45	5.35	5.55
4	530	65.05	65.25	65.00	5.50	5.40	5.65

Table 3.12 – Strips' dimensions after pre-straining

Strip number	l_0 [mm]	Width [mm]			Thickness [mm]		
		Section A	Section B	Section C	Section A	Section B	Section C
1	557	46.75	46.70	46.95	5.35	5.15	5.25
2	554	47.65	48.10	47.40	5.35	5.25	5.35
3	540	64.95	64.25	64.35	5.40	5.25	5.35
4	554	64.10	64.25	64.00	5.40	5.20	5.40

Based on these dimensions, the real strain can be estimated as follows:

$$\varepsilon_{real} = \frac{l_0^{after} - l_0^{before}}{l_0^{before}} \quad (3.4)$$

The results of the estimations are presented in Table 3.13.

Table 3.13 - Estimation of the strain, according to the strips' measurements

Strip number	ϵ_{real} [%]	Relative Error [%]
1	5.49	8.5
2	4.92	18.0
3	2.08	30.7
4	4.53	24.5

3.8 Microstructure of the 3% and 6% pre-strained steels

After the pre-straining operation, the microstructure of the plastically deformed steel was observed and compared with the microstructure of the material in the as-received condition. For that purpose, small metallographic specimens were cut from the 3% and 6% pre-strained strips. The metallographic procedures for surface preparation referred in section 3.4.2 were replicated for these samples.

The microstructure for strip number 3 is represented in Figure 3.27, Figure 3.28 and Figure 3.29. It can be observed the presence of several slipped grains in the L and LT directions, resulting from the pre-straining operation. This trend is more pronounced in the direction of application of the pre-strain.

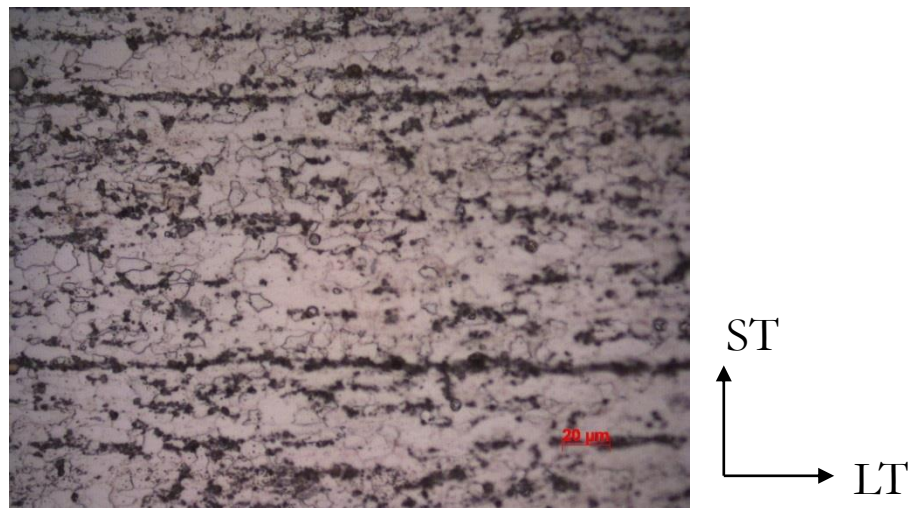


Figure 3.27 - Microstructure of strip number 3 with 3% pre-strain level in LT-ST direction

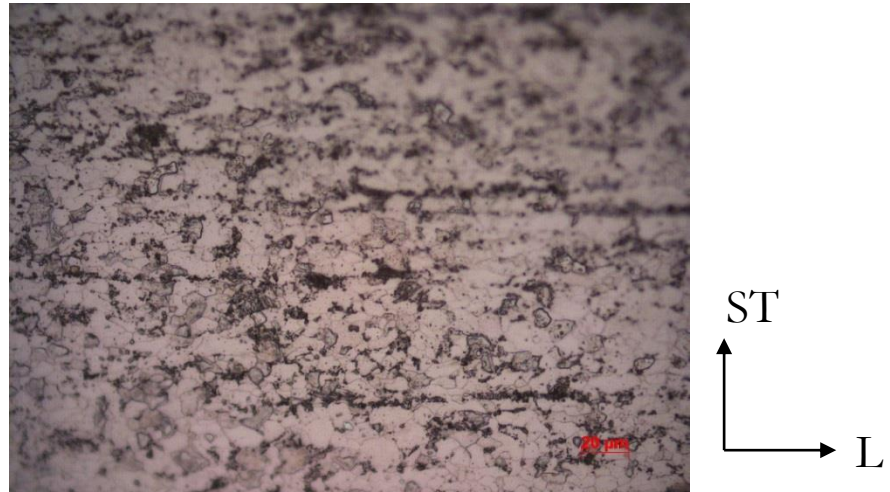


Figure 3.28 - Microstructure of strip number 3 with 3% pre-strain level in L-ST direction

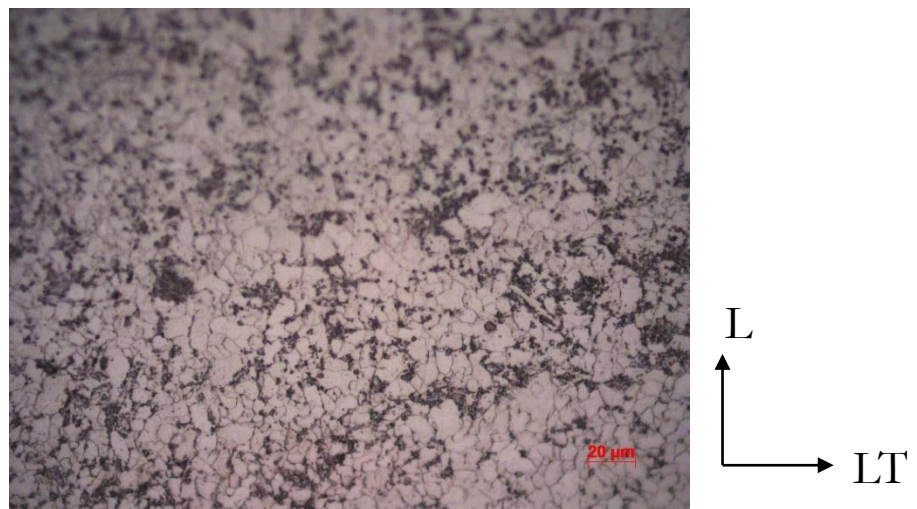


Figure 3.29 - Microstructure of strip number 3 with 3% pre-strain level in LT-L direction

Figure 3.30, Figure 3.31 and Figure 3.32 present the microstructure of the 6% pre-strained strip. For the 6% pre-strained samples, the occurrence of slipped grains both in the L and LT directions was higher when compared to the 3% pre-strained samples.

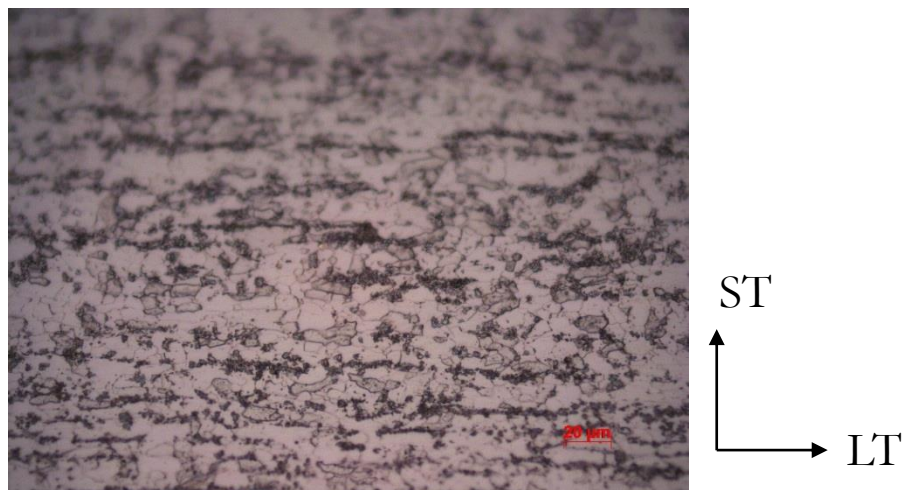


Figure 3.30 - Microstructure of strip number 2 with 6% pre-strain level in LT-ST direction

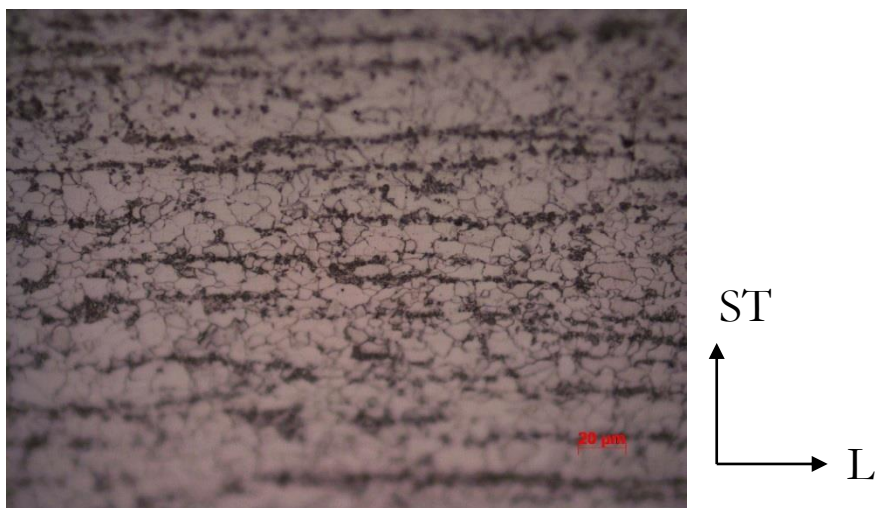


Figure 3.31 - Microstructure of strip number 2 with 6% pre-strain level in L-ST direction

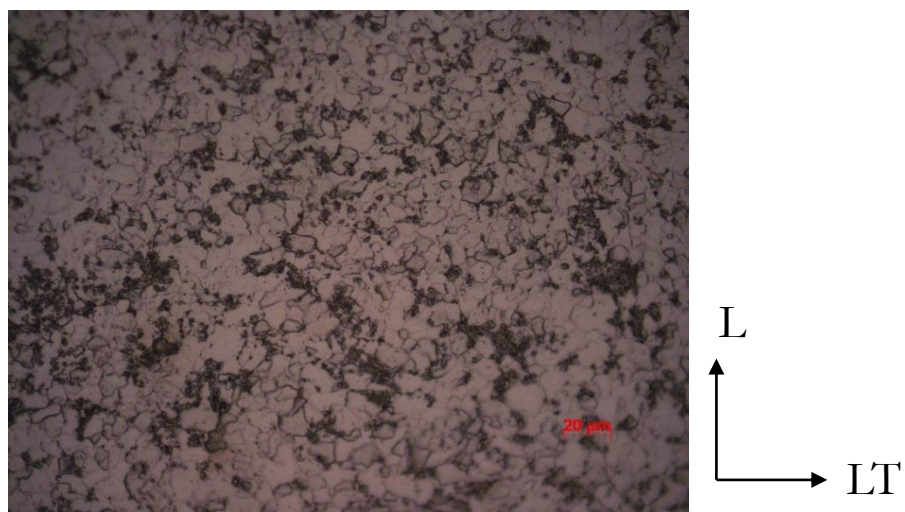


Figure 3.32 - Microstructure of strip number 2 with 6% pre-strain level in LT-L direction

3.9 Specimens' machining

After the steel strips had been subjected to the plastic pre-strain, the specimens were machined. In this section, the machining sequence of the specimens is described. First of all, the non pre-strained and pre-strained specimens were extracted from the original steel sheet and deformed steel strips, respectively. This extraction was performed according to the planning presented in Figure 3.17. For this operation, a band saw (Figure 3.33) was used which enabled good precision in the cut and minimized the excess of material.



Figure 3.33 - Band saw used for the extraction of the specimens

Afterwards, the specimens' side surfaces were machined by a milling according to the dimensions specified in the technical drawings. The work was performed in the universal milling machine shown in Figure 3.34.

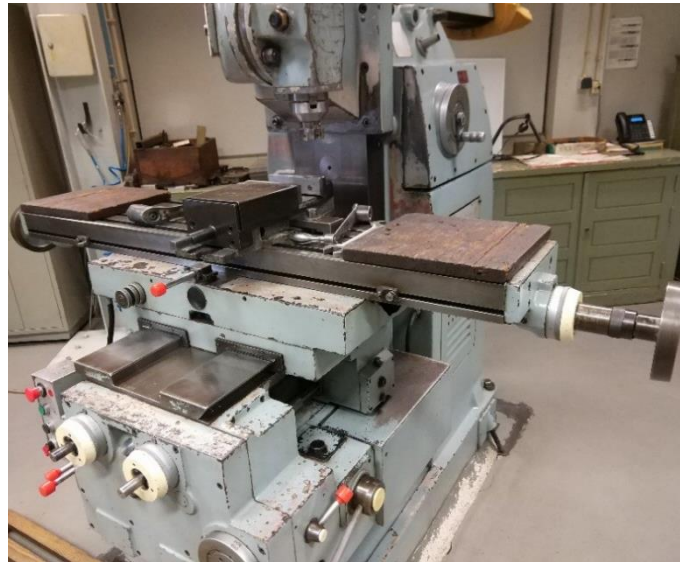


Figure 3.34 - Universal milling machine

After obtaining the final dimensions for the lateral surfaces, it was possible to correctly position the various holes of the specimens. Therefore, the next step was to drill the holes in the specimens. The drilling machine is illustrated in Figure 3.35.

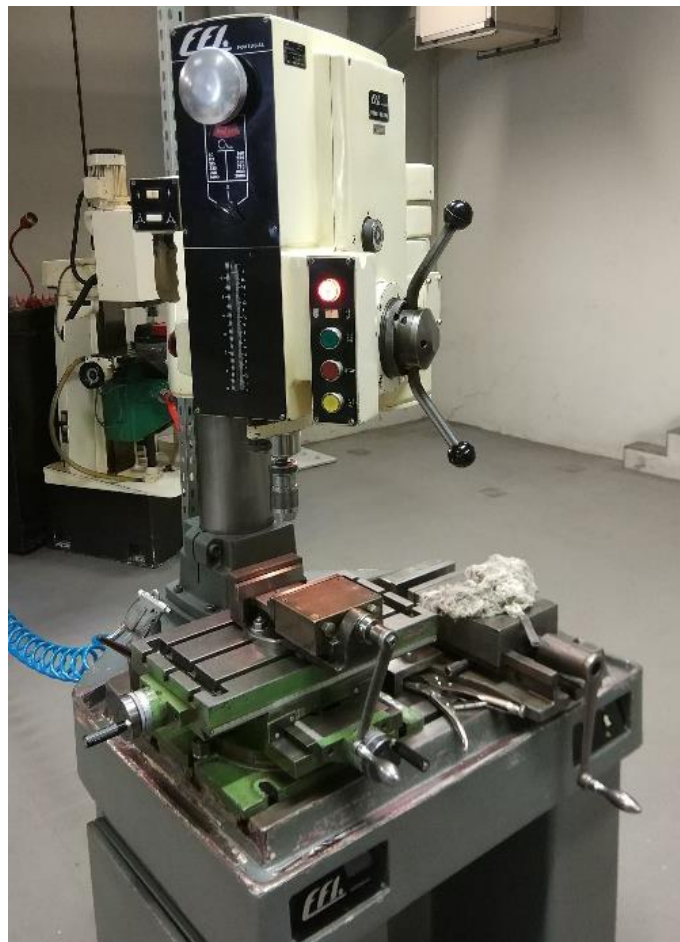


Figure 3.35 - Drilling machine

Subsequently, the initial notch was introduced in the specimen by milling. In this process, a cylindrical milling cutter with a 60° angle was employed. Once again, the universal milling machine was used. Finally, all specimens were grinded at the same time to ensure equal thickness. The operation was executed by the flat grinding machine represented in Figure 3.36. The CT specimens were further drilled in order to produce the additional M3 holes.



Figure 3.36 - Grinding machine

After machined, all specimens were covered with oil to preserve original conditions and prevent oxidation until the beginning of the experimental tests.

Figure 3.37 and Figure 3.38 present the machined CT specimens used in this work.

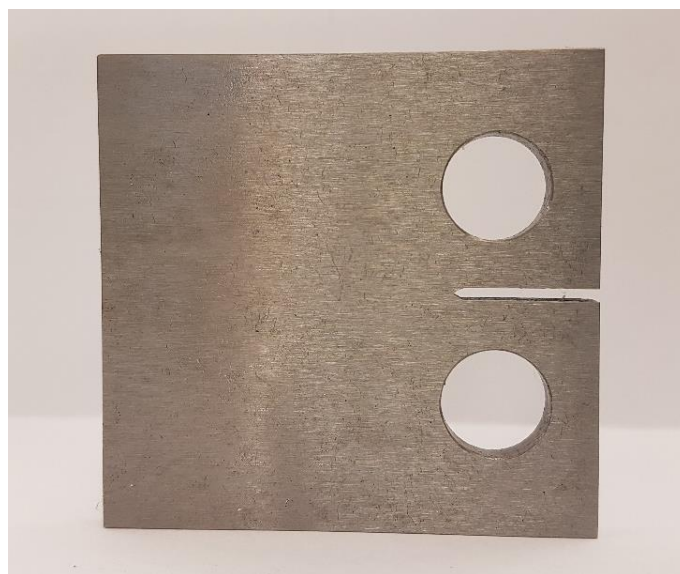


Figure 3.37 - Compact tension specimen

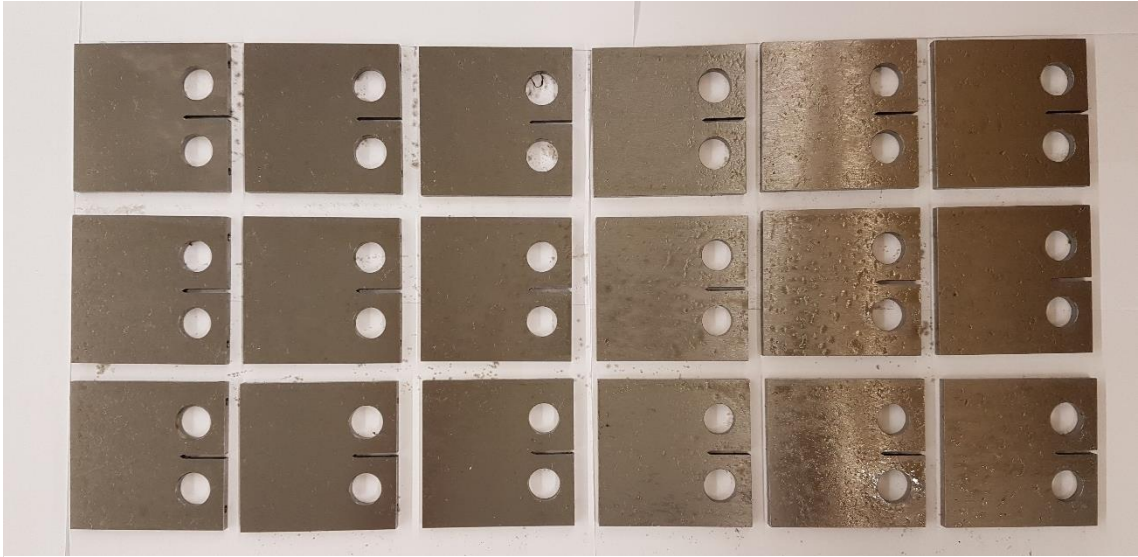


Figure 3.38 - All compact tension specimens machined in this work

The CTS specimens manufactured for this research are shown in Figure 3.39 and Figure 3.40.

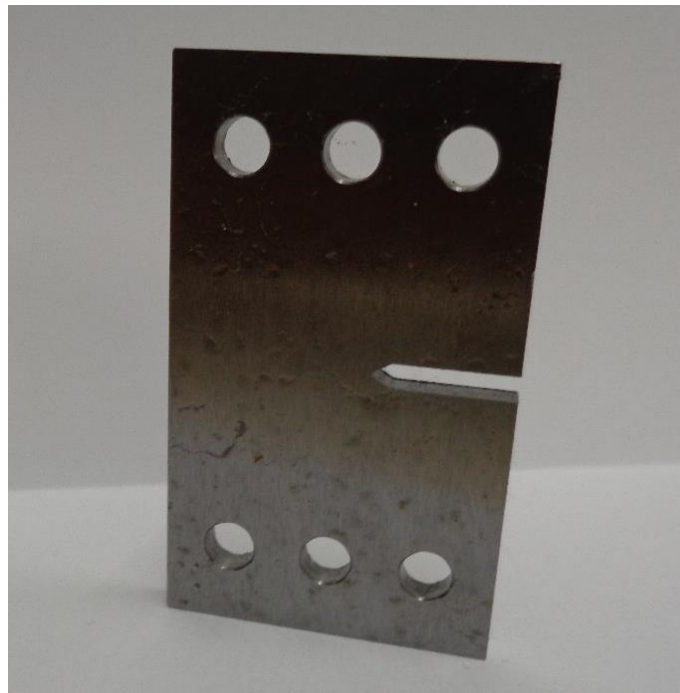


Figure 3.39 - Compact tension shear specimen



Figure 3.40 - All compact tension shear specimens machined in this work

3.10 Mixed mode (I+II) fatigue crack propagation tests

The mixed mode (I+II) fatigue crack propagation tests were conducted at Faculty of Mechanical Engineering of the Wrocław University of Science and Technology, Poland. The experiments were performed with the CTS specimen associated to a suitable loading device. The loading device, shown in Figure 3.41, enables the application of pure mode I, pure mode II or mixed mode loading to the CTS specimen using a uniaxial testing machine.

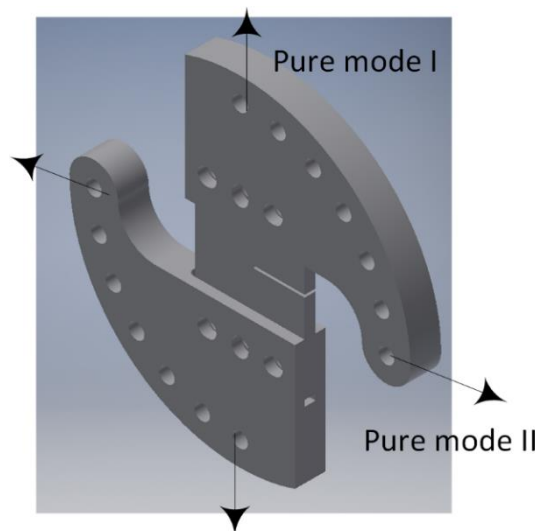


Figure 3.41 - Design of the loading device

The loading condition can be changed by varying the loading angle α , which is the angle between the longitudinal axis of the specimen and direction of the load applied by the uniaxial testing machine (Figure 3.42).

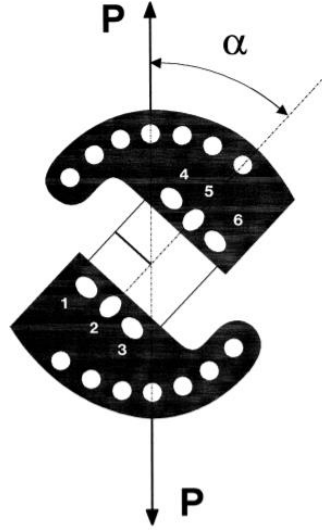


Figure 3.42 - Definition of loading angle (Rikards et al. 1998)

The pure mode I and pure mode II loading conditions are produced for $\alpha = 0^\circ$ and $\alpha = 90^\circ$, respectively.

For $0^\circ < \alpha < 90^\circ$, a mixed mode loading is achieved. The experimental setup implemented in this study is based on the mixed mode fracture testing technique developed by Richard (1985).

The specimen and loading device are connected through six rigid pins. It should be noted that the loading device has elongated holes, while the CTS specimen contains round ones (Figure 3.42).

The external holes (1, 3, 4 and 6) are elongated in the direction parallel to the crack so that only the forces perpendicular to the crack are transmitted from the loading device to the specimen. In other words, only the mode I loading components act on that hole. On the other hand, the middle holes (2 and 5) are elongated perpendicularly to the crack to ensure that only the mode II loading components are transmitted.

The fatigue tests were conducted under load control mode in a MTS 810 Uniaxial servohydraulic tensile testing machine, equipped with a 100 kN load cell. This machine was connected to a computer for control and data acquisition. The loads were applied using a sinusoidal waveform with a frequency of 10 Hz. During the tests, the stress ratio (R), maximum and minimum loads (F_{max} and F_{min}) were kept constant.

Prior to the proper crack propagation test, all CTS specimens were precracked under uniaxial mode I loading ($\alpha = 0^\circ$). The precracking procedure consisted of:

- Cyclic loading with $F_{max} = 5.5 \text{ kN}$ and $R = 0.1$ during 150.000 cycles;
- Cyclic loading with $F_{max} = 4.5 \text{ kN}$ and $R = 0.1$ during 50.000 cycles.

During the precracking procedure, it was carefully ensured that the center of the specimen was aligned with the load application line. An eccentrically loaded specimen can result in different crack lengths on the face and back surfaces of the specimen. In this case, the differences were not greater than 5-6%.

The measurement stand during precrack procedure is shown in Figure 3.43. The typical precrack crack length increment was between 1.3 mm and 1.6 mm.



Figure 3.43 - CTS specimen during precracking procedure (pure mode I)

After the precracking procedure, the proper experiment was performed under mixed mode loading conditions with two different stress ratio values ($R=0.05$ and $R=0.5$).

The tests conditions for each stress ratio are listed in Table 3.14. Constant amplitude loading was employed.

Table 3.14 - Loading conditions for the fatigue crack growth tests

	F_{max} [N]	F_{min} [N]	f [Hz]
$R = 0.05$	5000	250	10
$R = 0.5$	9500	4750	

Moreover, for each stress ratio, two loading angles were employed: $\alpha = 30^\circ$ and $\alpha = 60^\circ$. During the experimental tests, crack length was measured and number of cycles were registered. The crack length was cyclically monitored using a travelling DinoLite microscope with digital image processing (measurement) system with polarization. The measurement setup is shown in Figure 3.44.

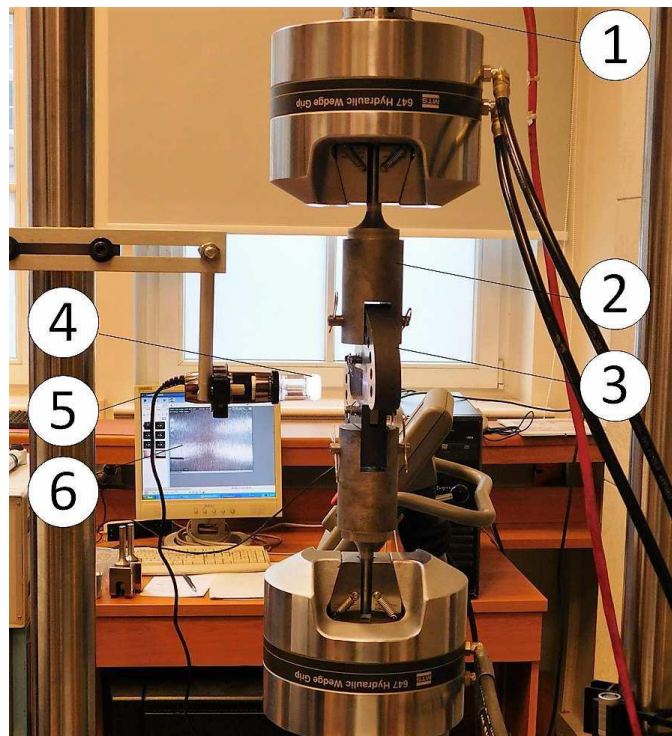


Figure 3.44 - Fatigue test stand for mode I+II, experimental setup: 1 – 100 kN MTS load cell, 2 – clevis, 3 – CTS specimen holder, 4 – light source with polarized filters, 5 – DinoLite microscope, 6 – integrated measurements system operated by the PC and FI

4 Results and discussion

4.1 Initial experimental results

The results of the experimental tests performed on the P355NL1 steel revealed the crack propagation path, number of cycles to failure (N_f) and crack length versus number of cycles (a vs. N) data. The a vs. N experimental data were fitted using exponential functions.

- $\alpha = 30^\circ$

The experimentally determined fatigue crack paths with initial measurements (necessary for simulations) for $\alpha = 30^\circ$ are shown in Figure 4.1, Figure 4.2, Figure 4.3, Figure 4.4 and Figure 4.5.

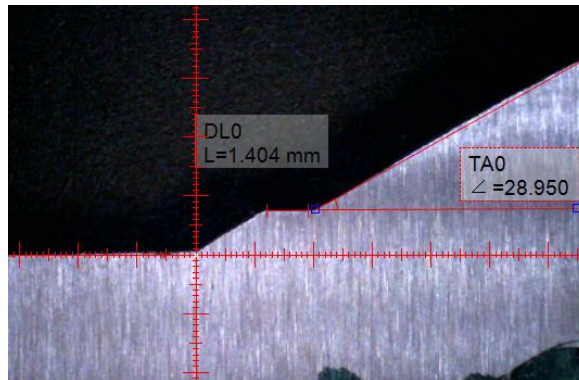


Figure 4.1 - Fatigue crack path of specimen I_30_0.05_1 (non pre-strained, R=0.05)

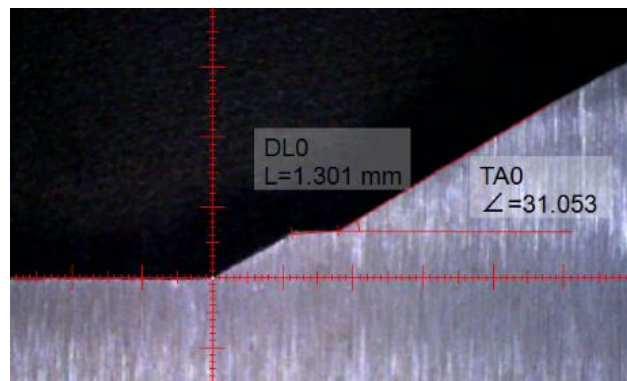


Figure 4.2 - Fatigue crack path of specimen II_30_0.05_1 (pre-strained, R=0.05)

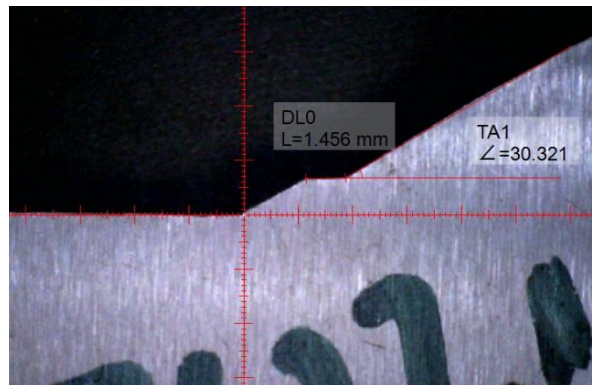


Figure 4.3 -Fatigue crack path of specimen II_30_0.05_2 (pre-strained, R=0.05)

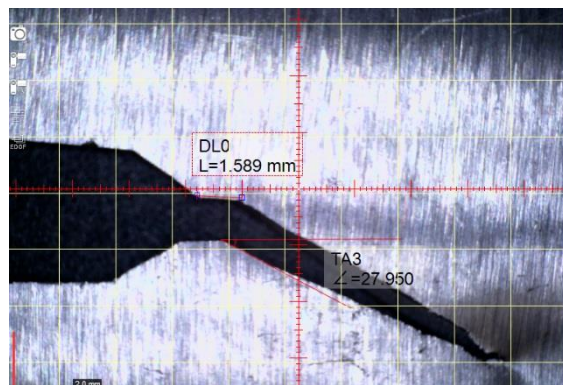


Figure 4.4 - Fatigue crack path of specimen I_30_0.5_1 (non pre-strained, R=0.5)

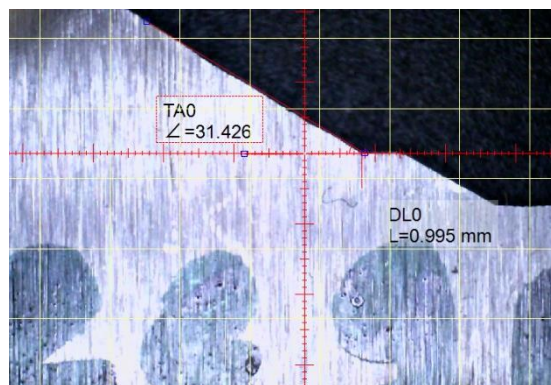


Figure 4.5 - Fatigue crack path of specimen II_30_0.5_1 (pre-strained, R=0.5)

The experimental test results obtained for $\alpha = 30^\circ$ are available in Table 4.1.

Table 4.1 - Initial test results of the CTS specimens for $\alpha=30^\circ$ (I-non pre-strained, II – pre-strained)

Specimen	F_{max} [N]	F_{min} [N]	N_f [cycles]	Crack initiation angle [°]	Pre-crack length [mm]	Total initial crack length [mm]
I_30_0.05_1	5000	250	261105	29	1.4	17.4+1.4=18.8
II_30_0.05_1	5000	250	247211	31.1	1.3	17.4+1.3=18.7
II_30_0.05_2	5000	250	265125	30.3	1.46	17.4+1.46=18.86
I_30_0.5_1	9500	4750	155028	28	1	17.27+1.1=18.37
II_30_0.5_1	9500	4750	229456	31.4	1.6	17.34+1.6=18.94

Figure 4.6 and Figure 4.7 show the a vs. N curve ($\alpha = 30^\circ$) for $R = 0.05$ and $R = 0.5$, respectively.

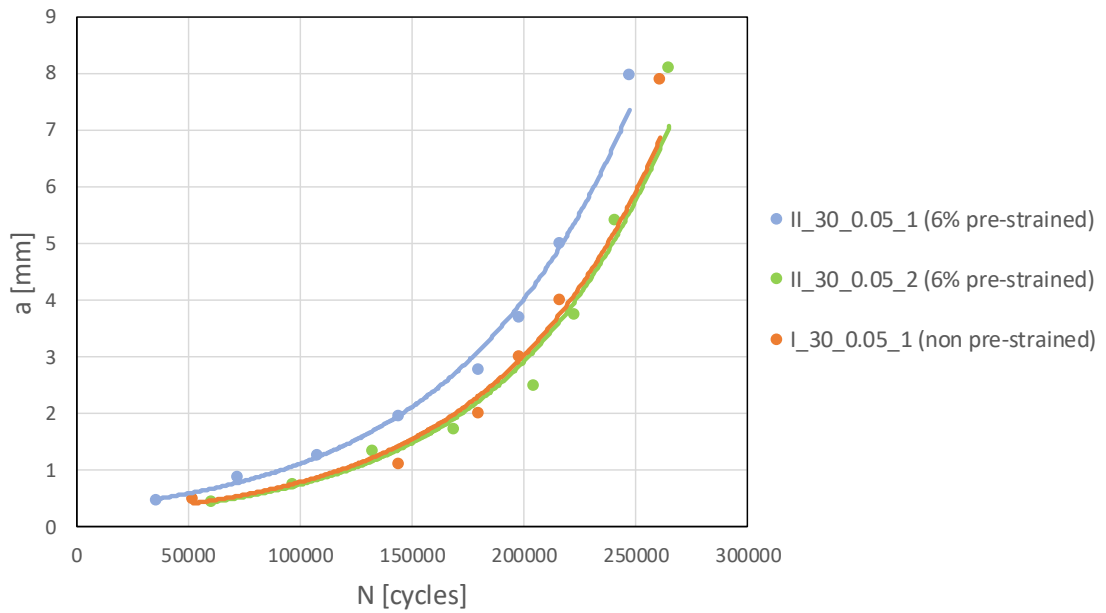


Figure 4.6 - Crack length versus number of cycles for $\alpha=30^\circ$ and $R=0.05$

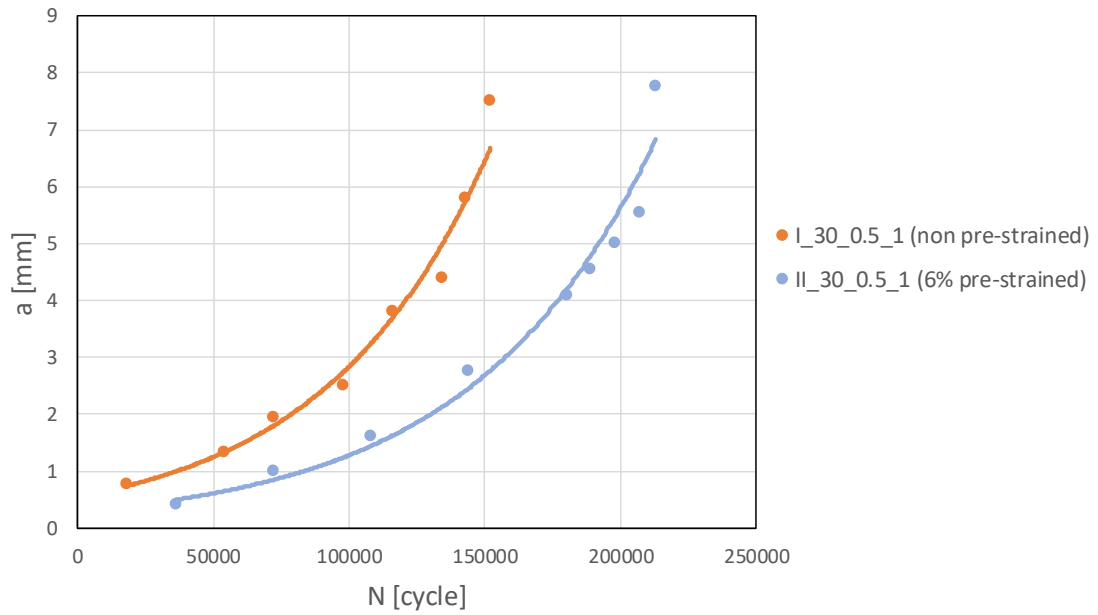


Figure 4.7 - Crack length versus number of cycles for $\alpha=30^\circ$ and $R=0.5$

▪ $\alpha = 45^\circ$

The experimentally determined fatigue crack paths with initial measurements (necessary for simulations) for $\alpha = 45^\circ$ are shown in Figure 4.8, Figure 4.9, Figure 4.10 and Figure 4.11.

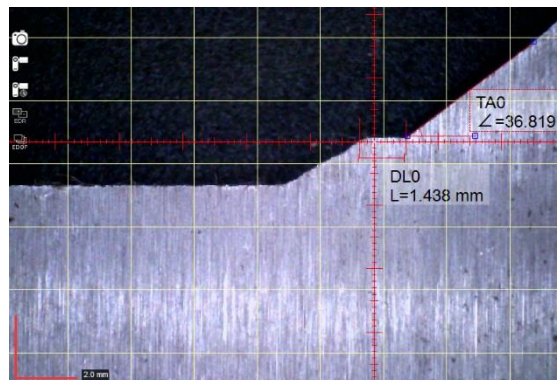


Figure 4.8 - Fatigue crack path of specimen I_45_0.05_1 (non pre-strained, $R=0.05$)

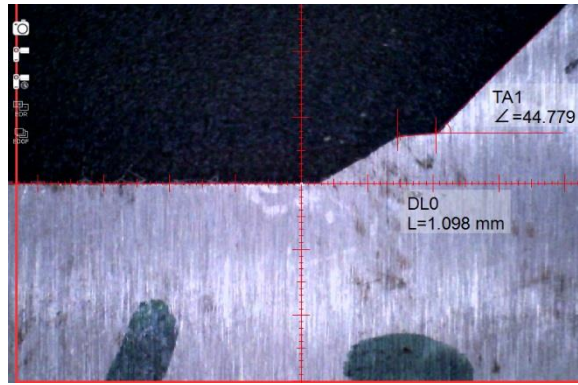


Figure 4.9 - Fatigue crack path of specimen II_45_0.05_1 (pre-strained, R=0.05)

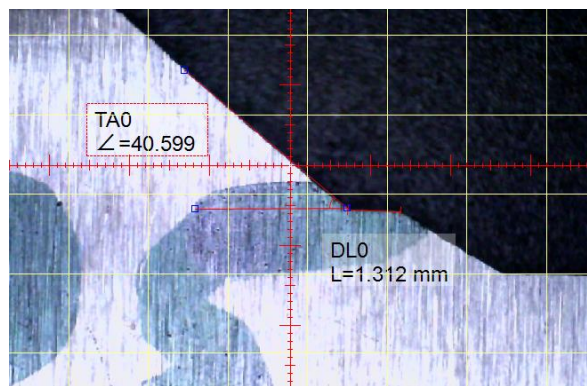


Figure 4.10 - Fatigue crack path of specimen I_45_0.5_1 (non pre-strained, R=0.5)

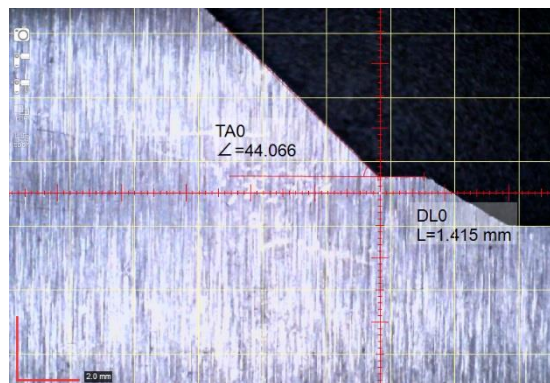


Figure 4.11 - Fatigue crack path of specimen II_45_0.5_1 (pre-strained, R=0.5)

The experimental test results obtained for $\alpha = 45^\circ$ are available in Table 4.2.

Table 4.2 - Initial test results of the CTS specimens for $\alpha=45^\circ$ (I-non pre-strained, II – pre-strained)

Specimen	$F_{\max}$ [N]	$F_{\min}$ [N]	N_f [cycles]	Crack initiation angle [$^\circ$]	Precrack length [mm]	Total initial crack length [mm]
I_45_0.05_1	5000	250	433000	36.8	1.4	17.04+1.4=18.44
II_45_0.05_1	5000	250	439540	44.7	1.1	17.14+1.5=18.54
I_45_0.5_1	9500	4750	162335	40.6	1.3	17.2+1.3=18.5
II_45_0.5_1	9500	4750	171104	44.1	1.4	17.3+1.4=18.7

Figure 4.12 and Figure 4.13 show the a vs. N curve ($\alpha = 45^\circ$) for $R = 0.05$ and $R = 0.5$, respectively.

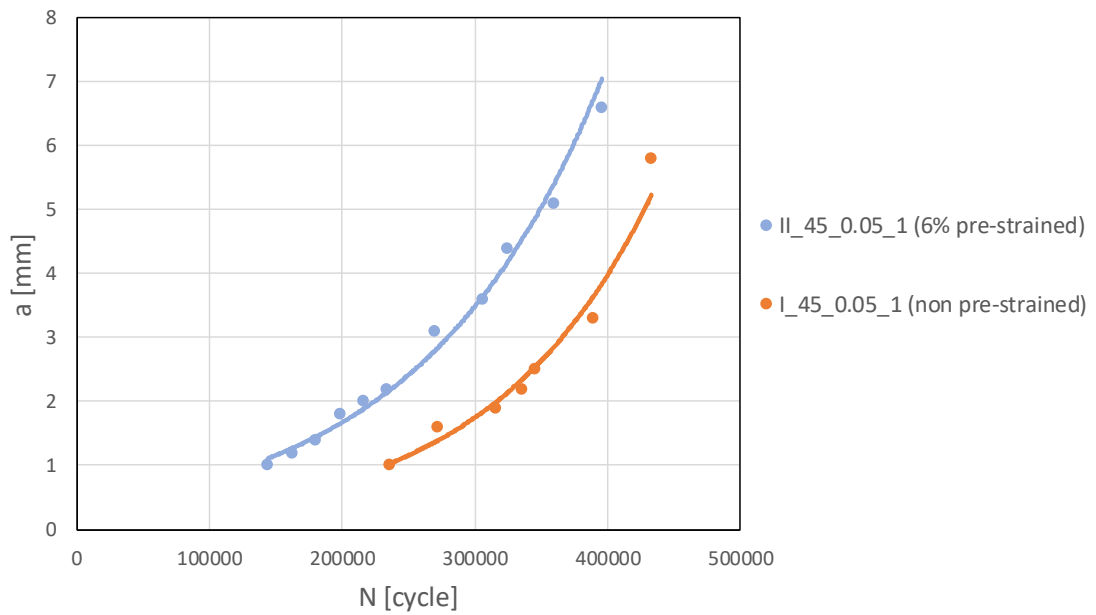


Figure 4.12 - Crack length versus number of cycles for $\alpha=45^\circ$ and $R=0.05$

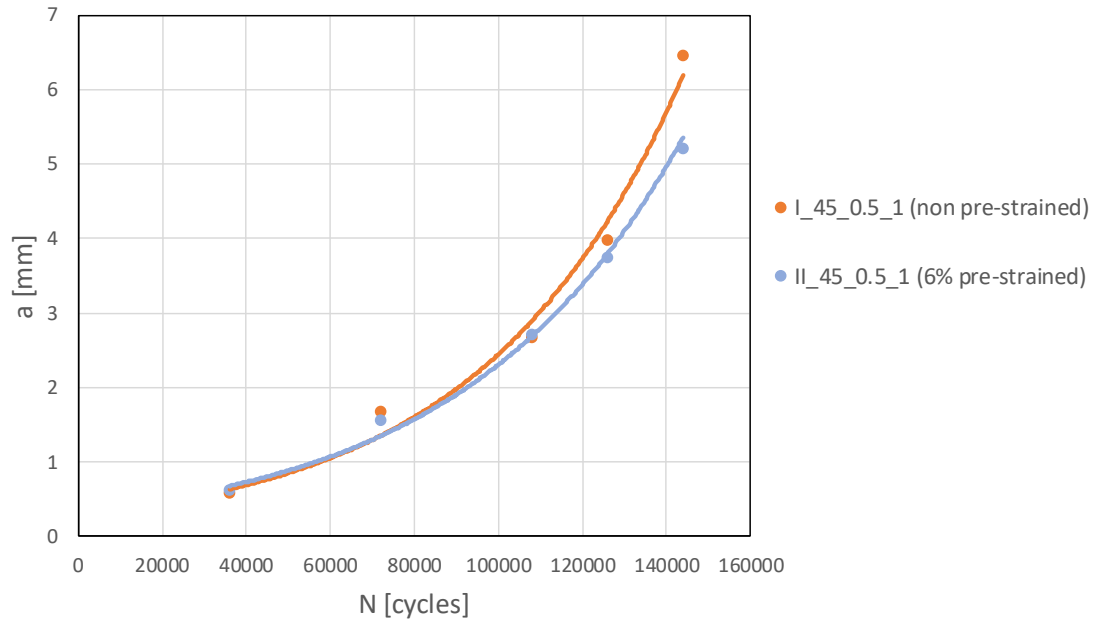


Figure 4.13 - Crack length versus number of cycles for $\alpha=45^\circ$ and $R=0.5$.

▪ $\alpha = 60^\circ$

The experimentally determined fatigue crack paths with initial measurements (necessary for simulations) for $\alpha = 60^\circ$ are shown in Figure 4.14, Figure 4.15, Figure 4.16 and Figure 4.17.

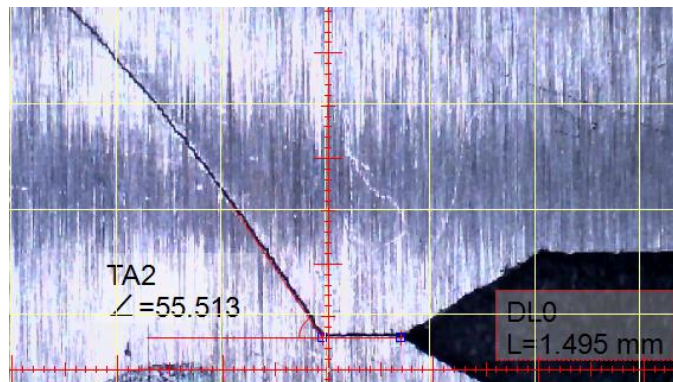


Figure 4.14 - Fatigue crack path of specimen I_60_0.05_1 (non pre-strained, $R=0.05$)

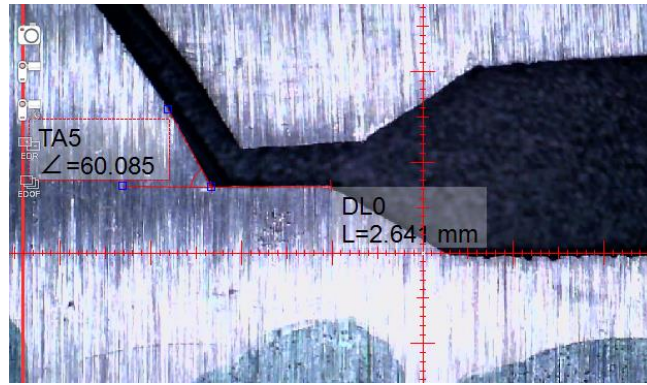


Figure 4.15 - Fatigue crack path of specimen II_60_0.05_1 (pre-strained, R=0.05)

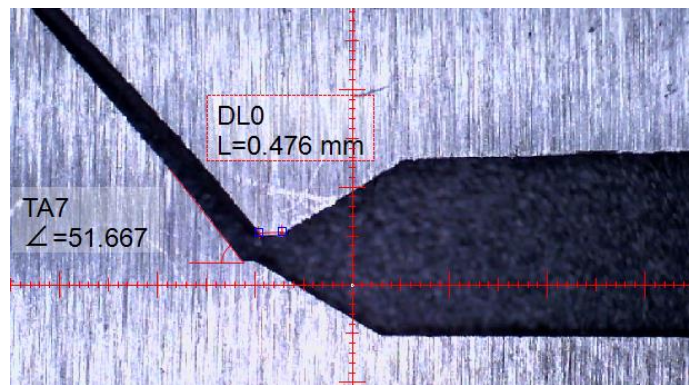


Figure 4.16 - Fatigue crack path of specimen I_60_0.5_1 (non pre-strained, R=0.5)

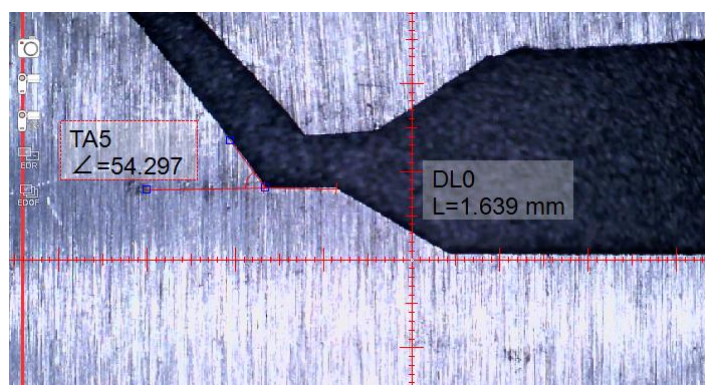


Figure 4.17 - Fatigue crack path of specimen II_60_0.5_1 (pre-strained, R=0.5)

The experimental test results obtained for $\alpha = 60^\circ$ are available in Table 4.3. It is noteworthy that the maximum and minimum loads for $R = 0.05$ were changed in this case.

Table 4.3 - Initial test results of the CTS specimens for $\alpha=60^\circ$ (I-non pre-strained, II – pre-strained)

Specimen	F_{max} [N]	F_{min} [N]	N_f [cycles]	Crack initiation angle [°]	Precrack length [mm]	Total initial crack length [mm]
I_60_0.05_1	8000	400	80014	55,5	1,51	17,11+1,51=18,62
II_60_0.05_1	8000	400	45192	60.1	2,6	17,4+2,6=20
I_60_0.5_1	9500	4750	340119	51,7	0,48	16.9+0,48=17,38
II_60_0.5_1	9500	4750	231878	54.3	1,64	17.4+1.64=19,04

Figure 4.18 and Figure 4.19 show the a vs. N curve ($\alpha = 60^\circ$) for $R = 0.05$ and $R = 0.5$, respectively.

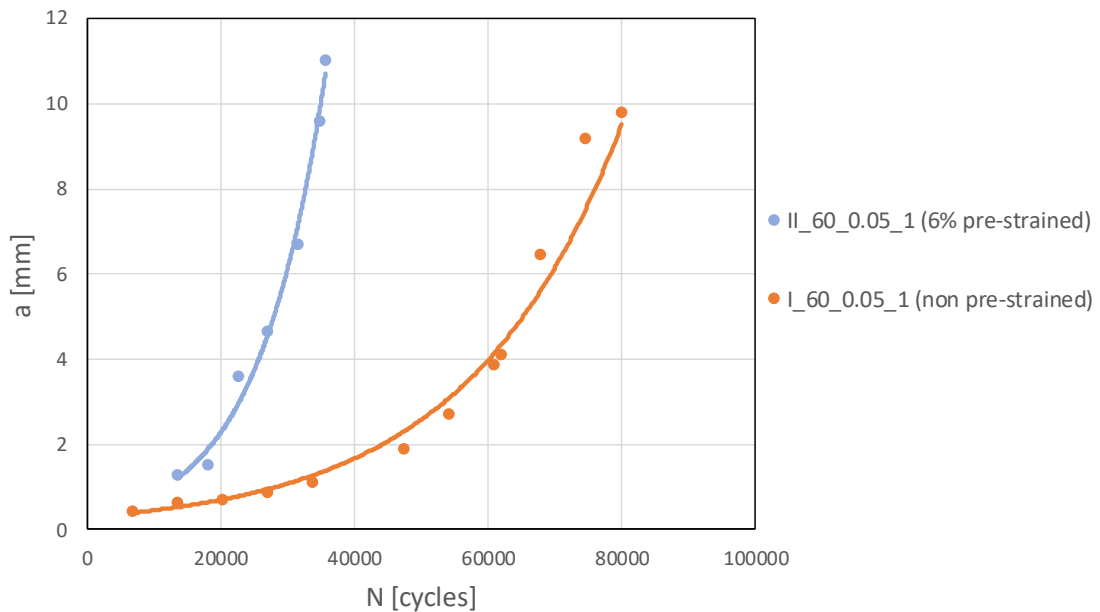


Figure 4.18 - Crack length versus number of cycles for $\alpha=60^\circ$ and $R=0.05$

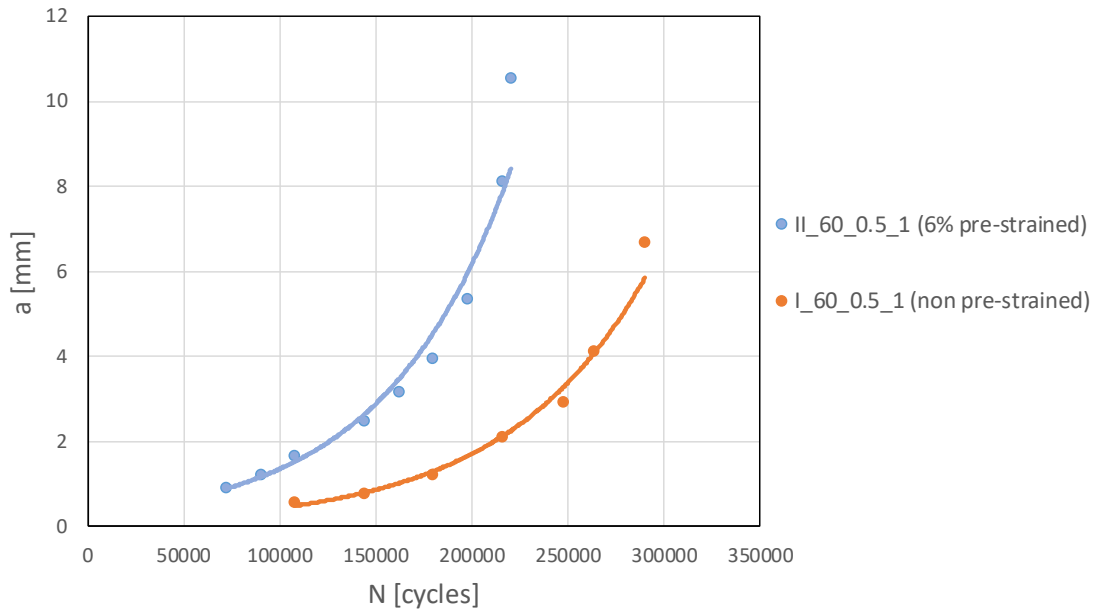


Figure 4.19 - Crack length versus number of cycles for $\alpha=60^\circ$ and $R=0.5$

From the results presented above, it is clear that, for the same loading conditions (α and R), the pre-strained specimens had higher crack initiation angles than the non pre-strained ones. This trend was verified for all different loading conditions.

For specimens with similar pre-crack length under the same loading conditions (e.g. I_45_0.5_1 and II_45_0.5_1), the pre-strained specimens appear to have an increased fatigue crack propagation life. However, comparing the results from the specimens II_30_0.05_1 and II_30_0.05_2 with the results from the specimen I_30_0.05_1, it is seen that the two specimens tested under the same conditions can lead to contrary conclusions about the pre-strain effect. Therefore, to confirm this inference it would be necessary to test more specimens under the same conditions in order to cover data scatter.

Moreover, for pre-strained or non pre-strained specimens under the same loading angle, the increase of the stress ratio caused a decrease in fatigue crack propagation life, as expected. This was not verified for $\alpha = 60^\circ$ because in this case the maximum and minimum loads for $R = 0.05$ were both increased, which led to a higher mean stress.

4.2 Numerical estimation of the stress intensity factor

Richard (1985) performed finite element analysis on the CTS specimen for the range $0.5 \leq a/W \leq 0.7$, thus obtaining solutions for K_I and K_{II} as a function of α and a/W . The stress intensity factors at the initial stage of test are given by the system of Equations (4.1) and (4.2).

$$K_I = \frac{F\sqrt{\pi a} \cos(\alpha)}{Wt \left(1 - \frac{a}{W}\right)} \sqrt{\frac{0.26 + 2.65 \left(\frac{a}{W} - a\right)}{1 + 0.55 \left(\frac{a}{W} - a\right) - 0.08 \left(\frac{a}{W} - a\right)^2}} \quad (4.1)$$

$$K_{II} = \frac{F\sqrt{\pi a} \sin(\alpha)}{Wt(1 - a/W)} \sqrt{\frac{-0.23 + 1.4(a/W - a)}{1 - 0.67 \left(\frac{a}{W} - a\right) + 2.08(a/W - a)^2}} \quad (4.2)$$

where F is the applied force, a is the crack length (mechanical notch+precrack), W is the specimen width, t is the specimen thickness and α is the loading angle. Equations (4.1) and (4.2) are only valid for the initial crack orientation and thus can only be applied to straight cracks. However, cracks usually change orientation under mixed mode loading. Figure 4.20 shows the broken specimens (60° load angle) with the kinked cracks typically observed in these experiments. For these conditions, the previously referred equations are inadequate. This was confirmed by Lesiuk *et al.* (2017) and Rozumek *et al.* (2017).



Figure 4.20 - Pre-strained (left) and non-prestrained (right) specimens after the crack growth test ($\alpha=60^\circ$, $R=0.5$).

Therefore, a numerical analysis is required to obtain the values of K_I and K_{II} under mixed mode loading conditions. Based on the crack paths obtained in the experiments, the stress intensity factors were computed using the Finite Element Method (FEM). The finite element software package used to build and analyze the model was Abaqus 6.11-2. For each point of the crack tip, K_I and K_{II} were calculated using J-integral. Six contours were used.

The material was considered to be homogeneous, isotropic with linear elastic behavior ($E = 205.2 \text{ GPa}$, $\nu = 0.275$). Plane stress conditions were assumed.

The uniaxial load F was decomposed into the various forces acting on the holes, as follows:

$$F_1 = F_6 = F \left(\frac{1}{2} \cos \alpha + \frac{c}{b} \sin \alpha \right) \quad (4.3)$$

$$F_2 = F_5 = F \sin \alpha \quad (4.4)$$

$$F_3 = F_4 = F \left(\frac{1}{2} \cos \alpha - \frac{c}{b} \sin \alpha \right) \quad (4.5)$$

where $c = b = 25.2 \text{ mm}$ for the specified geometry of the CTS specimen.

Figure 4.21 illustrates the loading and boundaries conditions considered in the numerical simulations.

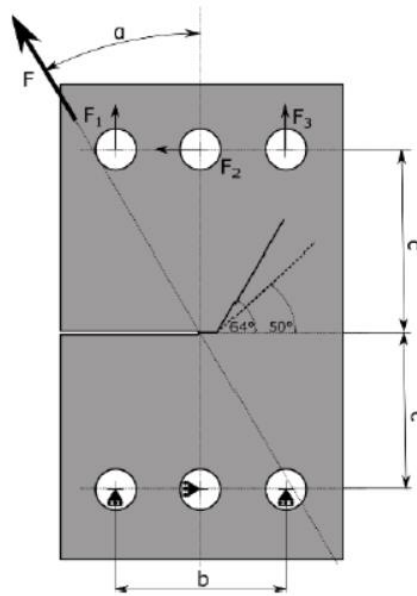


Figure 4.21 - Decomposition of the load and boundary conditions for the CTS specimen (Lesiuk et al. 2017)

The values of the local forces F_i acting through the pins in the holes, for the different loading conditions, are listed in Table 4.4.

Table 4.4 - Decomposition of the main uniaxial force F on the local forces F_i acting on the holes

α	R	F [N]	F_1 [N]	F_2 [N]	F_3 [N]
30°	0.05	5000	4665	2500	-335
		250	233	125	-17
	0.5	9500	8864	4750	-636
		4750	4431	2375	-318
45°	0.05	5000	5303	3536	-1768
		250	265	177	-88
	0.5	9500	10076	6718	-3359
		4750	5038	3358	-1679
60°	0.05	8000	8928	6928	-4928
		400	446	346	-246
	0.5	9500	10602	8227	-5852
		4750	5301	4114	-2926

In order to apply load and boundary conditions, the centers of the specimens' holes were defined as reference points (RP) and kinetically coupled to their respective hole (Figure 4.22). This enables the distribution of the load or constraint through the nodes defining the hole.

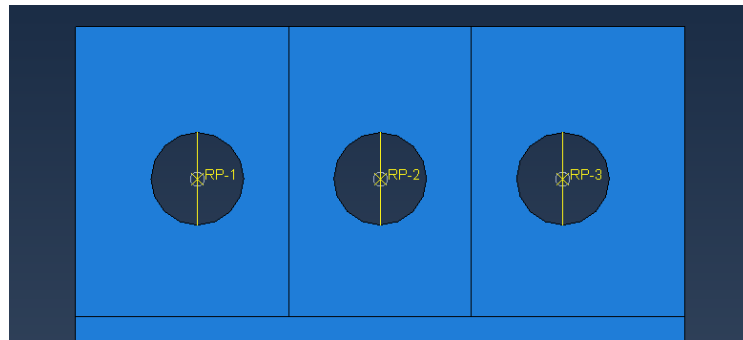


Figure 4.22 - Coupling between holes and its centers (RP)

The fatigue crack was modeled as stationary, using a constant iteration of 0.5 mm.

Figure 4.23 shows numerical models corresponding to increasing crack lengths.

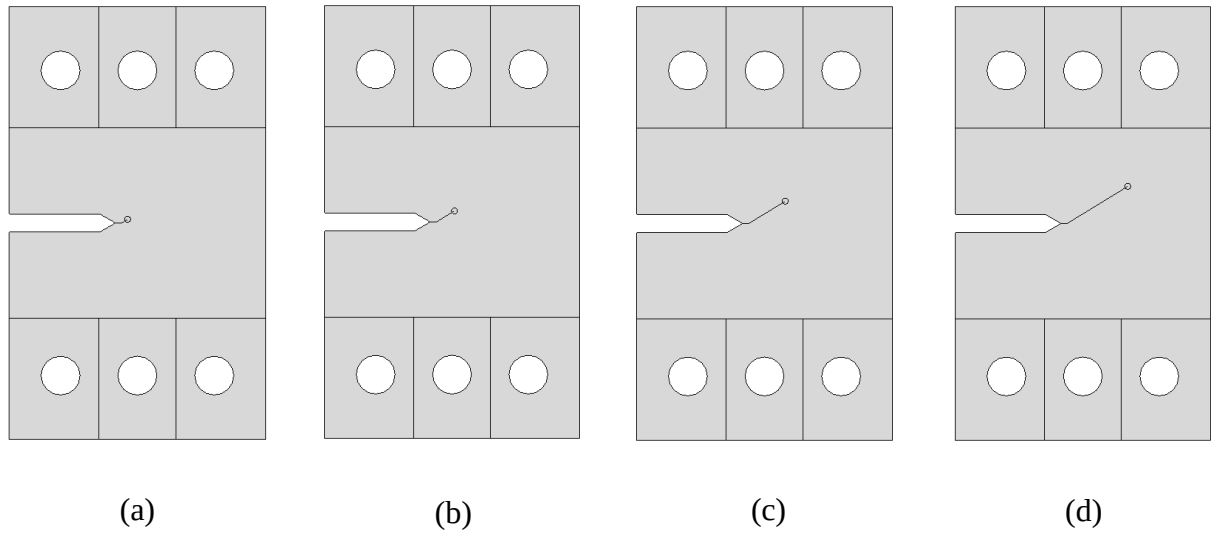


Figure 4.23 - Models for different crack lengths: (a) 1 mm (b) 3 mm (c) 6 mm (d) 10 mm

Stress intensity factors were obtained using the quarter points method. At the crack tip, collapsed quarter-point singular elements were used. In these elements, the mid-side nodes of the quadratic order elements are moved to quarter point positions to reproduce the singularity ($1/\sqrt{r}$) of the stress field at the crack tip. In the region around the crack tip, a sweep meshing technique was employed.

In the remaining areas of the CTS model, quadratic plane stress elements with reduced integration (CPS8R) were considered. These regions were meshed using free meshing method. The total number of elements and nodes varies between iterations. The denser mesh featured 7096 elements while the coarser mesh featured 12592 elements.

Figure 4.24 presents the finite element mesh of the CTS specimen model for a pre-crack length of 1 mm, a crack length of 4 mm and a crack initiation angle of 31.4° .

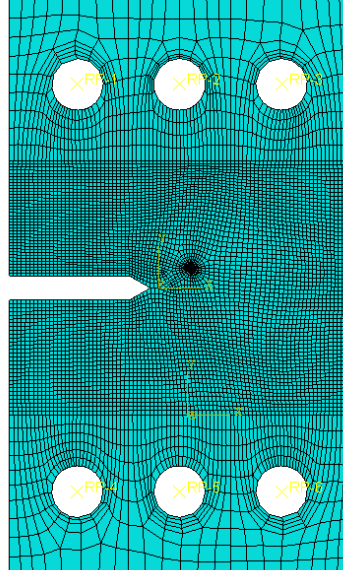


Figure 4.24 - Finite element mesh of a non pre-strained CTS specimen model ($\alpha=30^\circ$, $R=0.05$).

As previously mentioned, the values of K_I and K_{II} under mixed mode loading conditions were calculated by means of numerical simulations. Based on this output, it was possible to define both mode I and mode II stress intensity factor range (ΔK_I and ΔK_{II} , respectively), for each loading condition:

$$\Delta K_i = K_{i_{max}} - K_{i_{min}} \quad (4.6)$$

where $K_{i_{max}}$ and $K_{i_{min}}$ are the stress intensity factors corresponding to the case of maximum and minimum load, respectively.

After obtaining ΔK_I and ΔK_{II} , an equivalent stress intensity factor range (ΔK_{eq}) was calculated using the Huber-Mises criterion for proportional loading:

$$\Delta K_{eq} = \sqrt{\Delta K_I^2 + 3\Delta K_{II}^2} \quad (4.7)$$

This was the parameter used in the kinetic fatigue fracture diagrams.

Based on the numerical simulations described above, it was possible to obtain a set of data points (a , ΔK_{eq}). These points were fitted to curves with a 6th order polynomial.

The data points and fitting curves that correlate the crack length (a) with the equivalent stress intensity factor range (ΔK_{eq}) are presented in Figure 4.25, Figure 4.26, Figure 4.27, Figure 4.28, Figure 4.29 and Figure 4.30.

▪ **$\alpha = 30^\circ$**

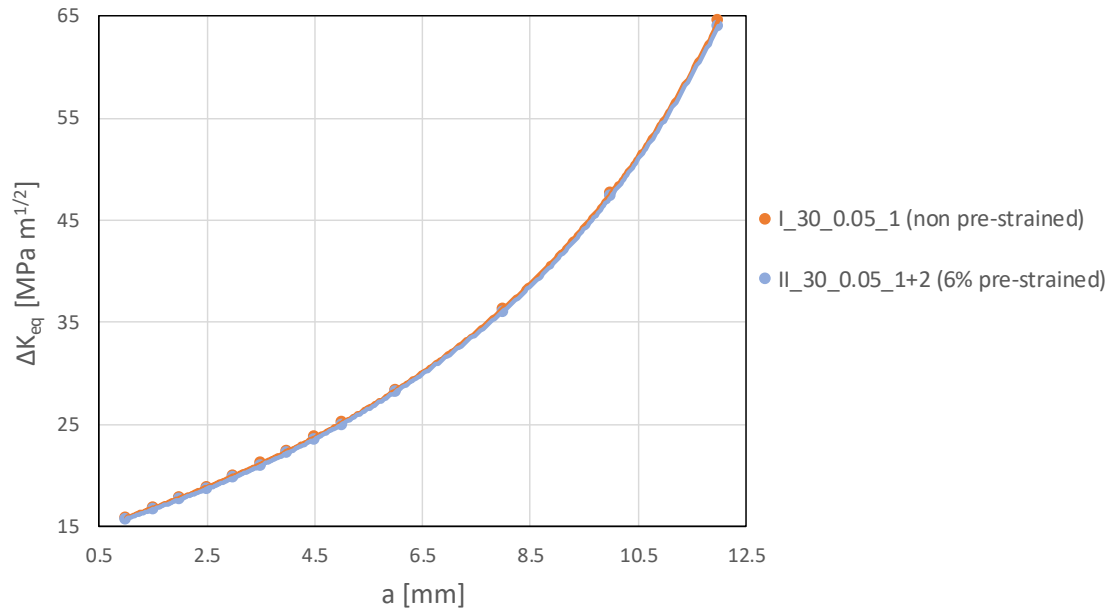


Figure 4.25 - ΔK_{eq} vs. a curve for $\alpha=30^\circ$ and $R=0.05$

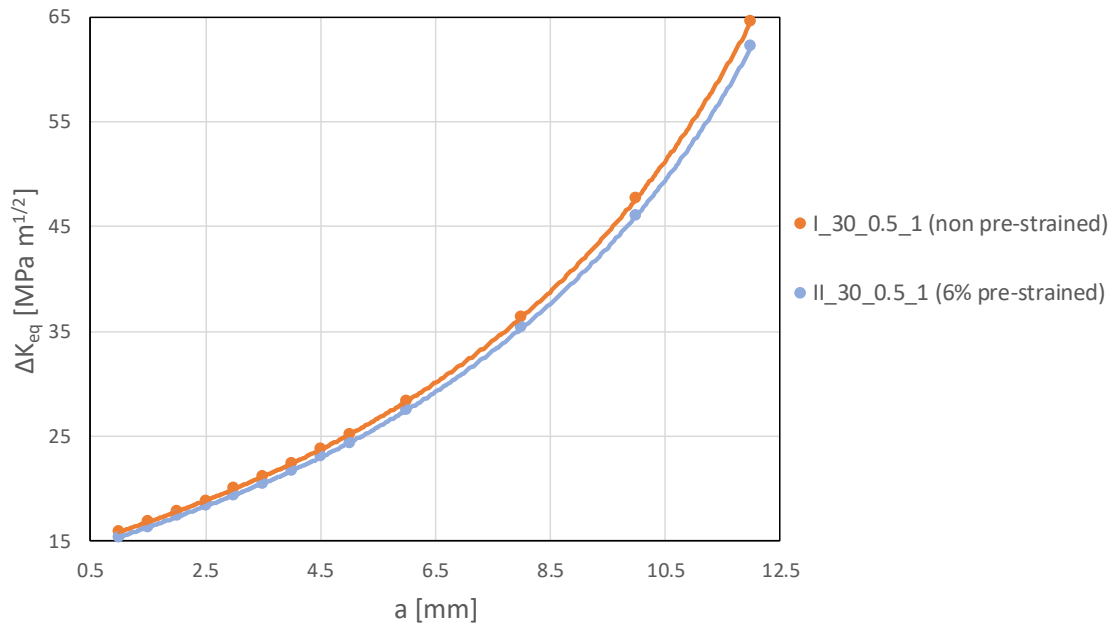


Figure 4.26 - ΔK_{eq} vs. a curve for $\alpha=30^\circ$ and $R=0.5$

▪ **$\alpha = 45^\circ$**

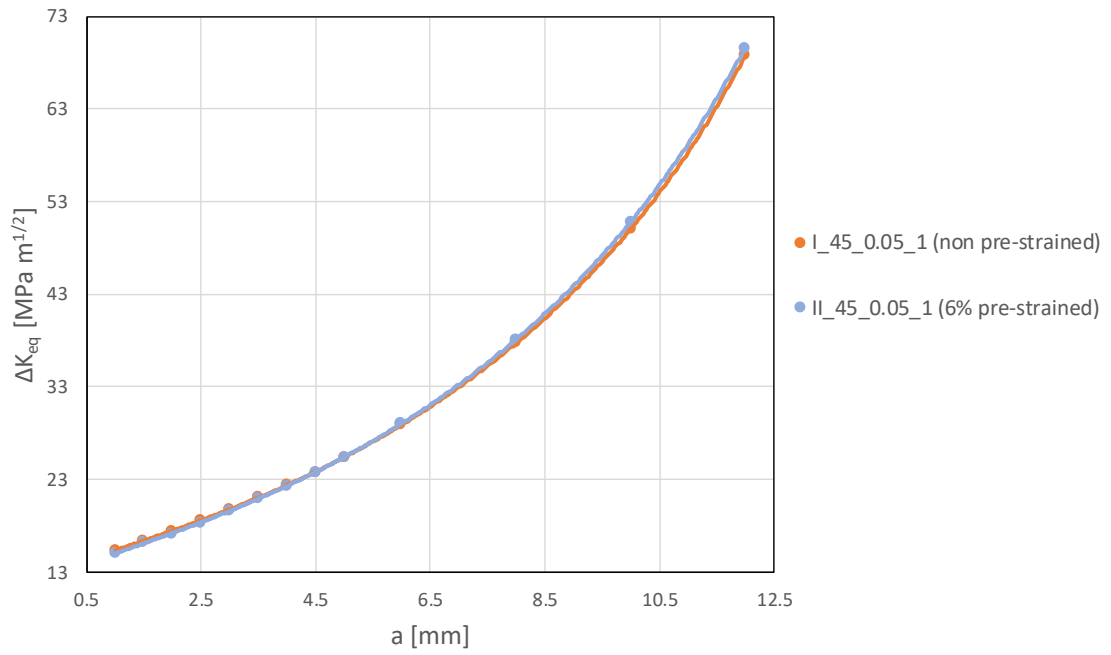


Figure 4.27 - ΔK_{eq} vs. a curve for $\alpha=45^\circ$ and $R=0.05$

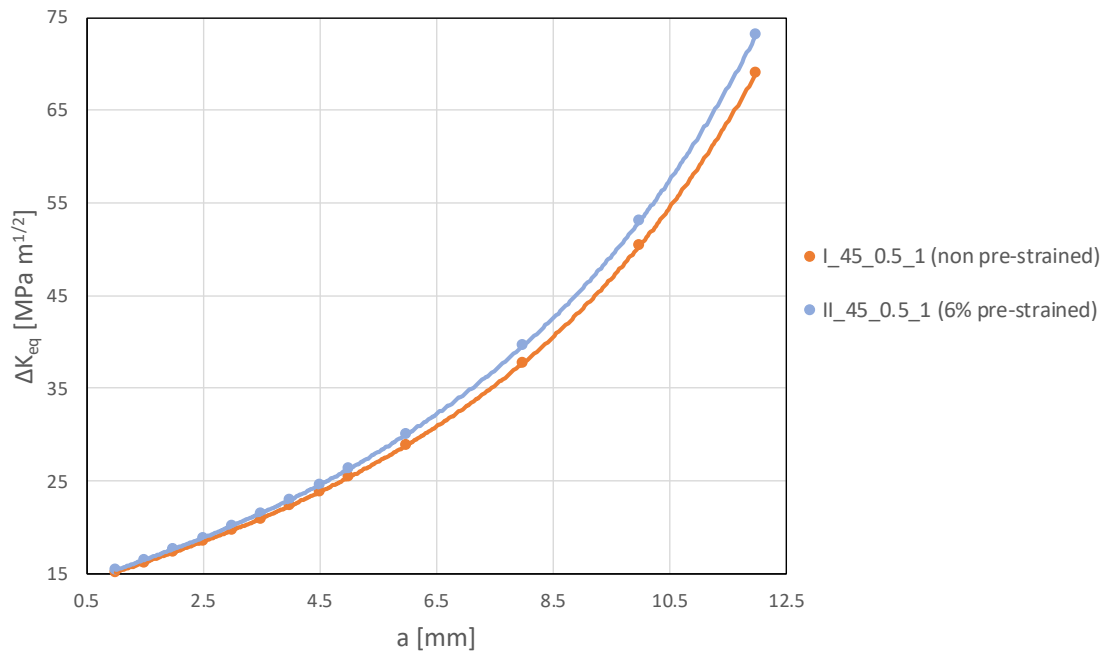


Figure 4.28 - ΔK_{eq} vs. a curve for $\alpha=45^\circ$ and $R=0.5$

▪ $\alpha = 60^\circ$

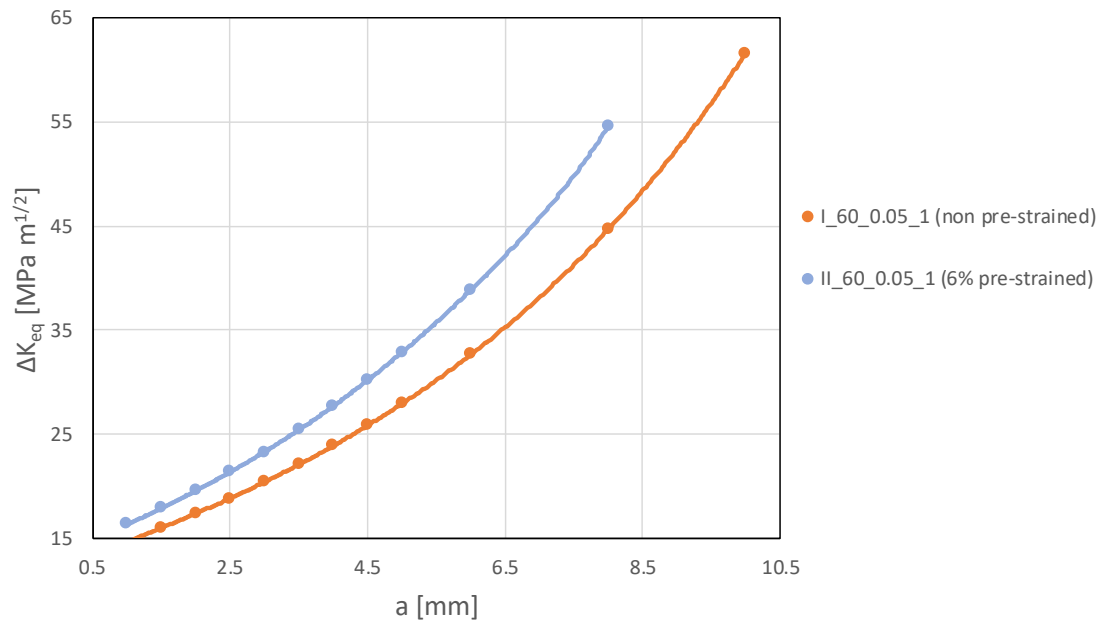


Figure 4.29 - ΔK_{eq} vs. a curve for $\alpha=60^\circ$ and $R=0.05$

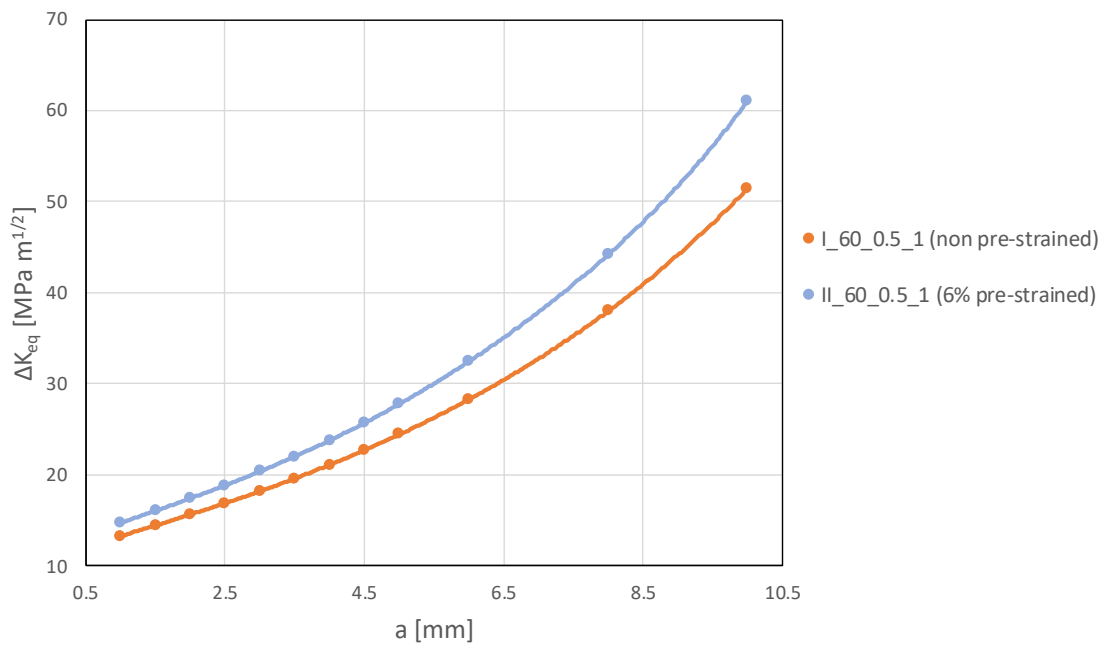


Figure 4.30 - ΔK_{eq} vs. a curve for $\alpha=60^\circ$ and $R=0.5$

4.3 Kinetic fatigue fracture diagrams

The kinetic fatigue fracture diagrams were built using the crack length versus number of cycles (a vs. N) data, which was determined in the experimental tests, and the equivalent stress intensity factor range versus crack length (ΔK_{eq} vs. a) data, which was determined via numerical simulations.

The crack growth rate da/dN was calculated based on the a vs. N data, as follows:

$$\frac{da}{dN}|_{a_i} \approx \frac{\Delta a}{\Delta N}|_{a_i} = \frac{a_i - a_{i-1}}{N_i - N_{i-1}} \quad (4.8)$$

where i is the number of the experimental point considered. For $i = 1$, i.e. for the first experimental point, a_{i-1} becomes a_0 , which means that there is no experimental data. In this case, an arbitrary crack length smaller than a_1 is defined as a_0 and the number of cycles N_0 is calculated using the equation of exponential curve that fits the experimental data.

The equivalent stress intensity factor range ΔK_{eq} was determined for the different crack lengths of the experimental data a_i , using the equation of the 6th order polynomial curve that fits the numerical data.

Finally, with the crack growth rate da/dN and the equivalent stress intensity factor range ΔK_{eq} for different crack lengths a_i , it was possible to plot points on the graph of da/dN vs. ΔK_{eq} . Each point corresponds to a different crack length a_i . These points were fitted to a curve with a power function.

4.3.1 Influence of pre-strain

▪ $\alpha = 30^\circ$

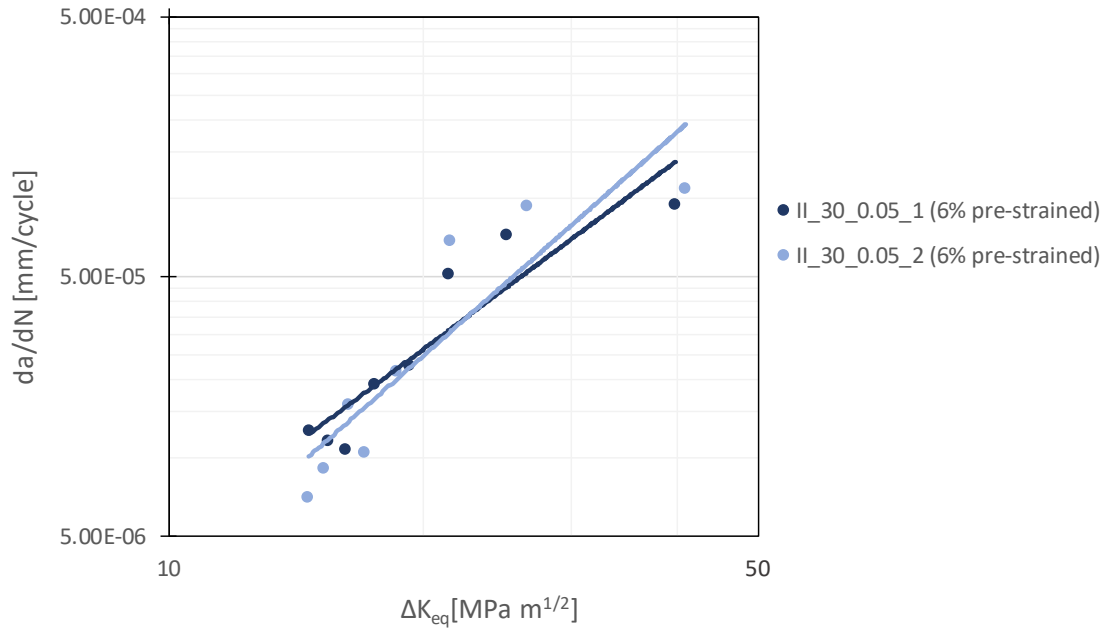


Figure 4.31 - Kinetic fatigue fracture diagram of two pre-strained specimens ($\alpha=30^\circ$ and $R=0.05$)

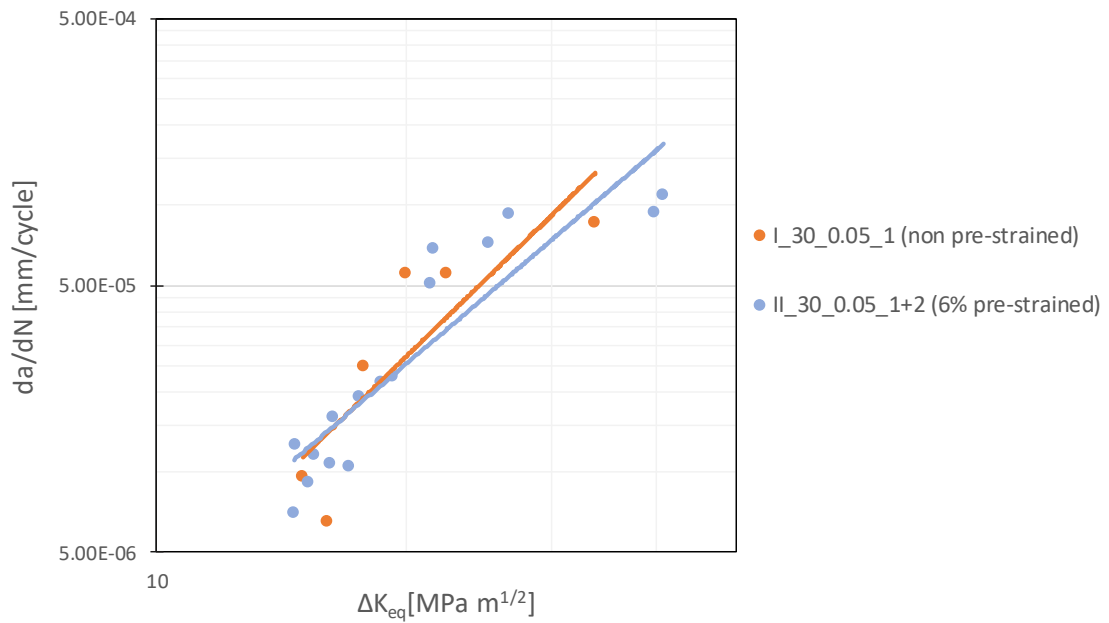


Figure 4.32 - Kinetic fatigue fracture diagram for $\alpha=30^\circ$ and $R=0.05$

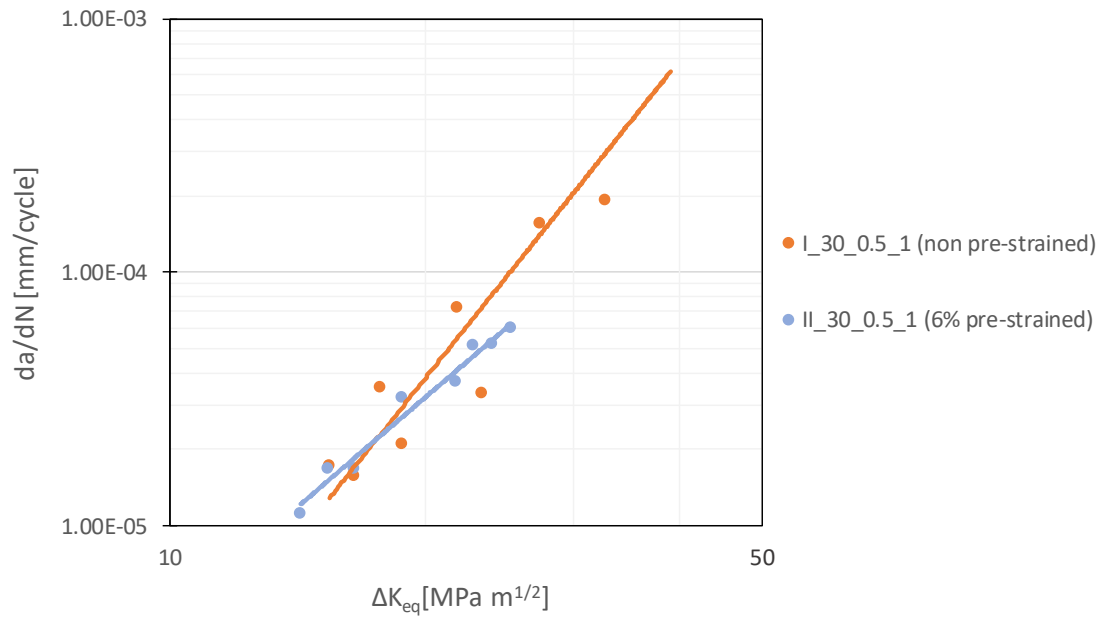


Figure 4.33 - Kinetic fatigue fracture diagram for $\alpha=30^\circ$ and $R=0.5$

▪ $\alpha = 45^\circ$

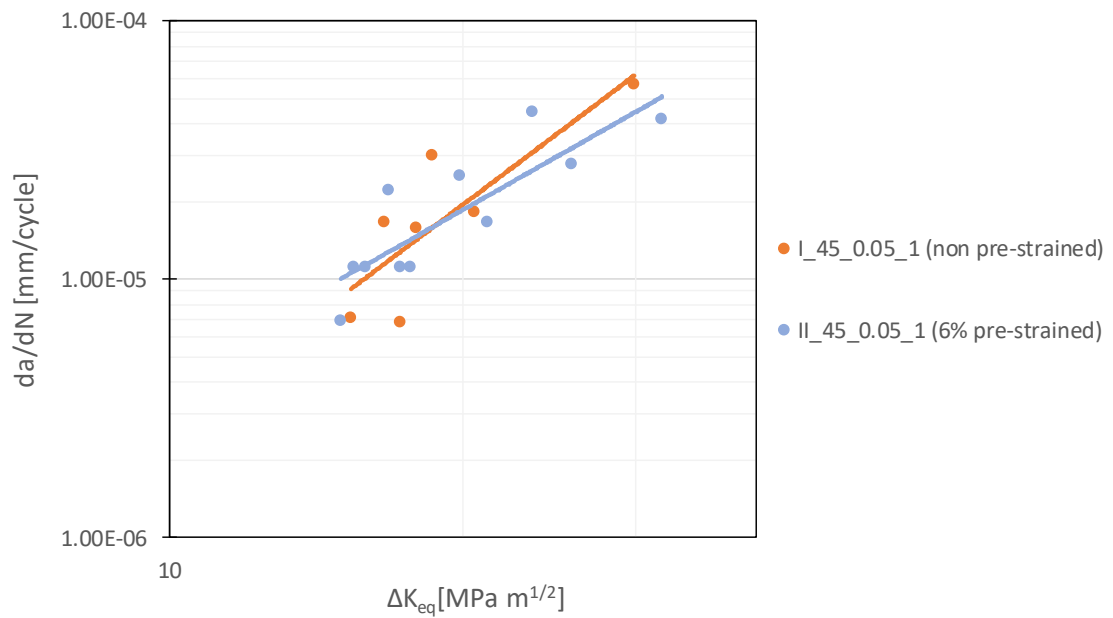


Figure 4.34 - Kinetic fatigue fracture diagram for $\alpha=45^\circ$ and $R=0.05$

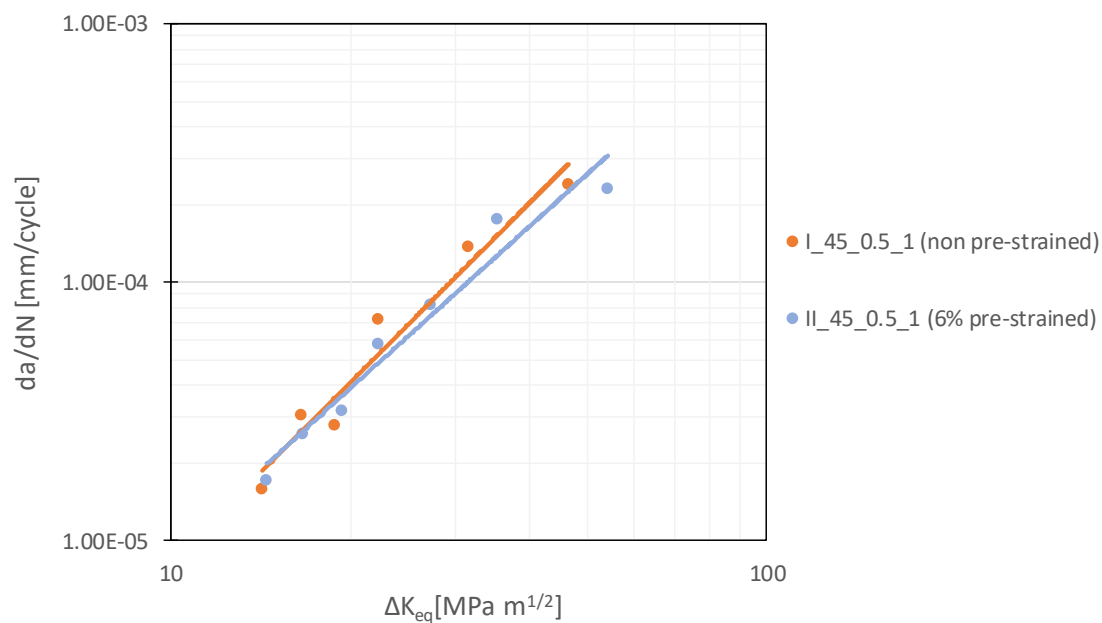


Figure 4.35 - Kinetic fatigue fracture diagram for $\alpha=45^\circ$ and $R=0.5$

▪ $\alpha = 60^\circ$

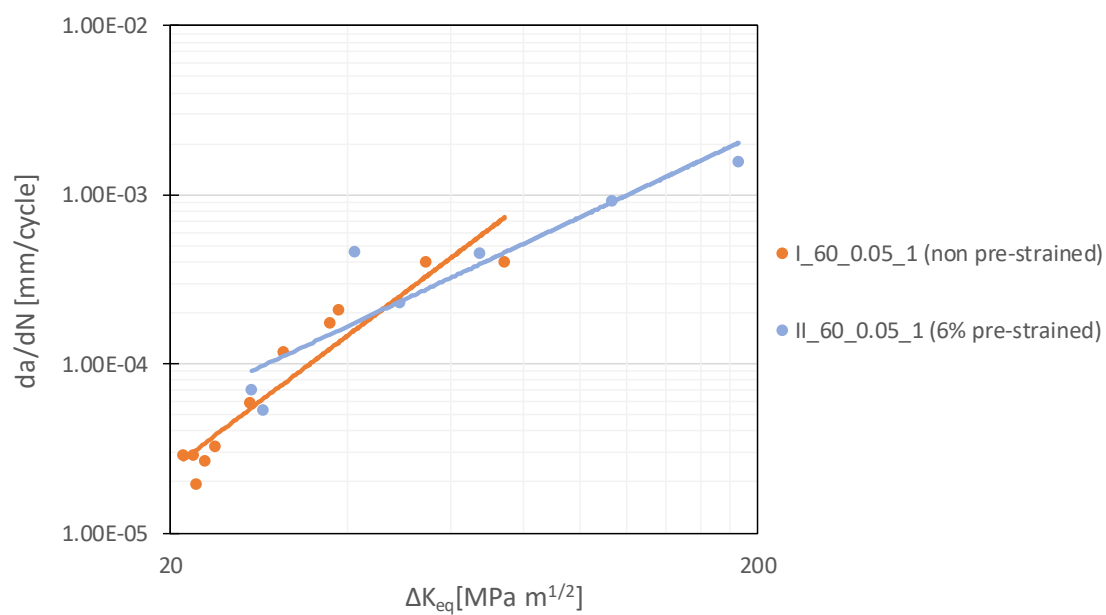


Figure 4.36 - Kinetic fatigue fracture diagram for $\alpha=60^\circ$ and $R=0.05$

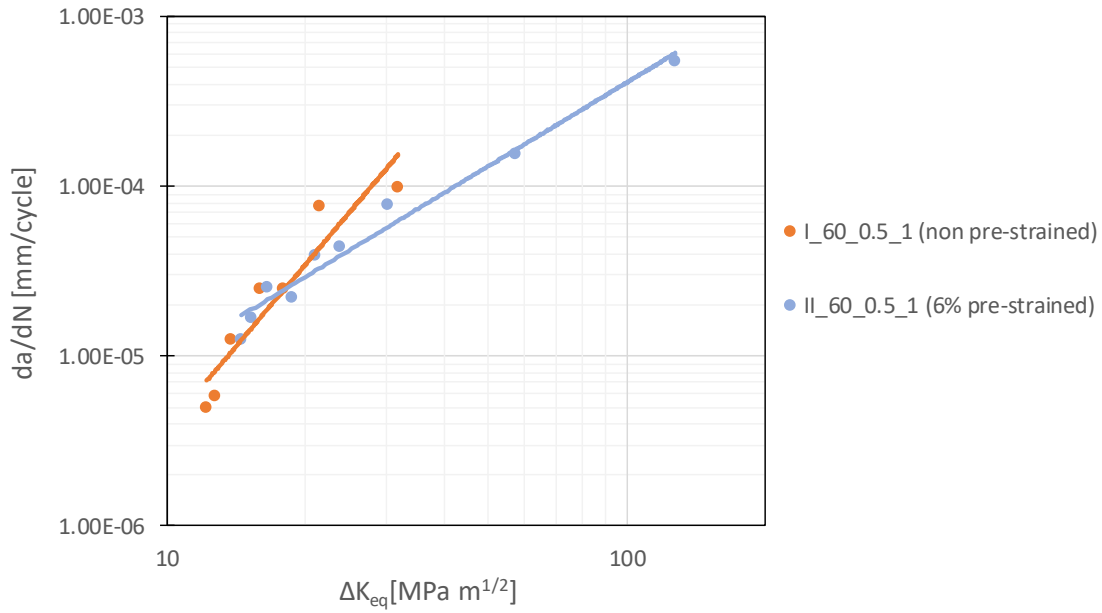


Figure 4.37 - Kinetic fatigue fracture diagram for $\alpha=60^\circ$ and $R=0.5$

Figure 4.31 presents the kinetic fatigue fracture diagrams for two specimens tested under the conditions (stress ratio, loading angle and pre-strain level). As we can see, the results for the two specimens were different and might lead to different conclusions when compared to the results of non pre-strained specimen showed in Figure 4.32. This shows the importance of testing various specimens under the same conditions in order to cover possible data scatter. In Figure 4.32, all experimental data (points) from the two pre-strained specimens were used for the curve fitting.

Looking at Figure 4.32, Figure 4.33, Figure 4.34, Figure 4.35, Figure 4.36 and Figure 4.37, it is clear that, in the Paris region, the curves for pre-strained specimens are below the non pre-strained ones, for all stress ratios and loading angles. This means that, under the same ΔK_{eq} , fatigue crack propagation rates are higher for the latter condition. Therefore, it can be concluded that pre-strain actually decelerates fatigue crack growth in the Paris region.

For very low ΔK_{eq} -values, the fitting curves for the pre-strained specimens are above the ones for the non pre-strained specimens, for all stress ratios and loading angles. This indicates that pre-strain might decrease fatigue crack growth threshold (ΔK_{th}). To confirm such inference, further testing would be required in the near-threshold region. However, similar results have been reported by Wang *et al.* (2009), Jiang and Chen (2012) and Nakajima *et al.* (1999).

4.3.2 Influence of stress ratio

▪ $\alpha = 30^\circ$

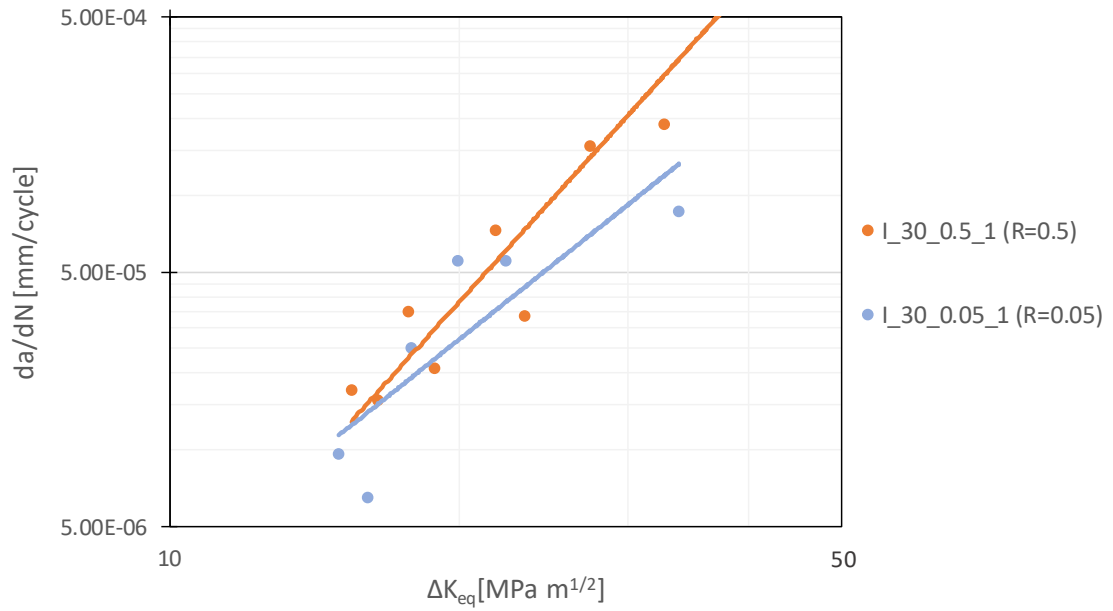


Figure 4.38 - Kinetic fatigue fracture diagram for $\alpha=30^\circ$ and non pre-strained specimens.

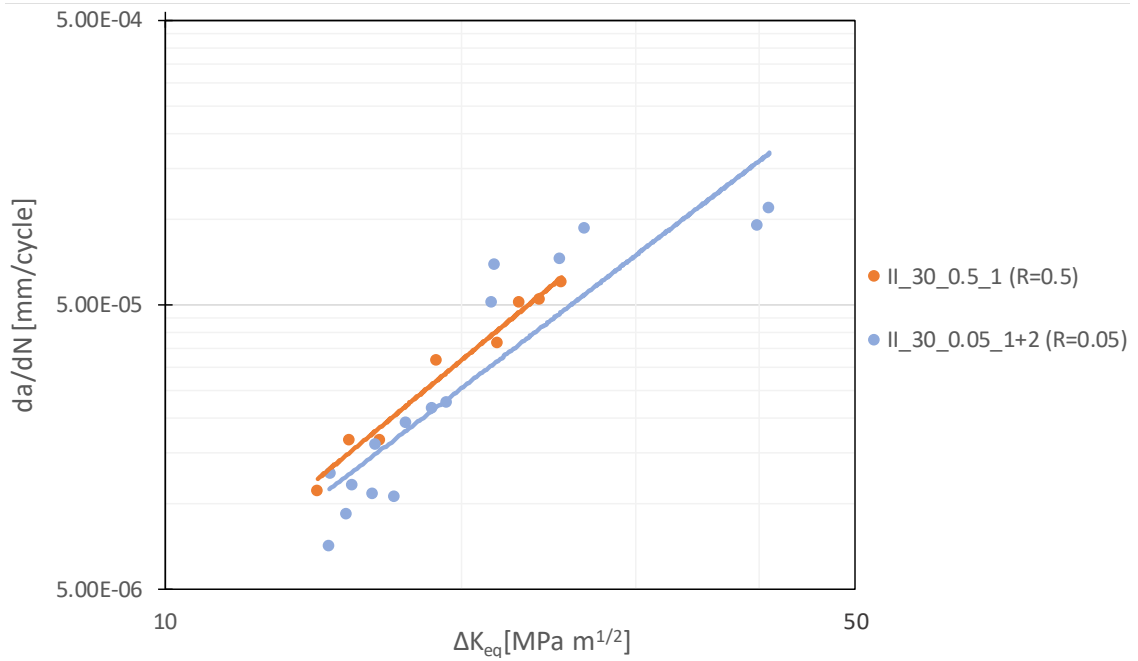


Figure 4.39 - Kinetic fatigue fracture diagram for $\alpha=30^\circ$ and pre-strained specimens.

▪ $\alpha = 45^\circ$

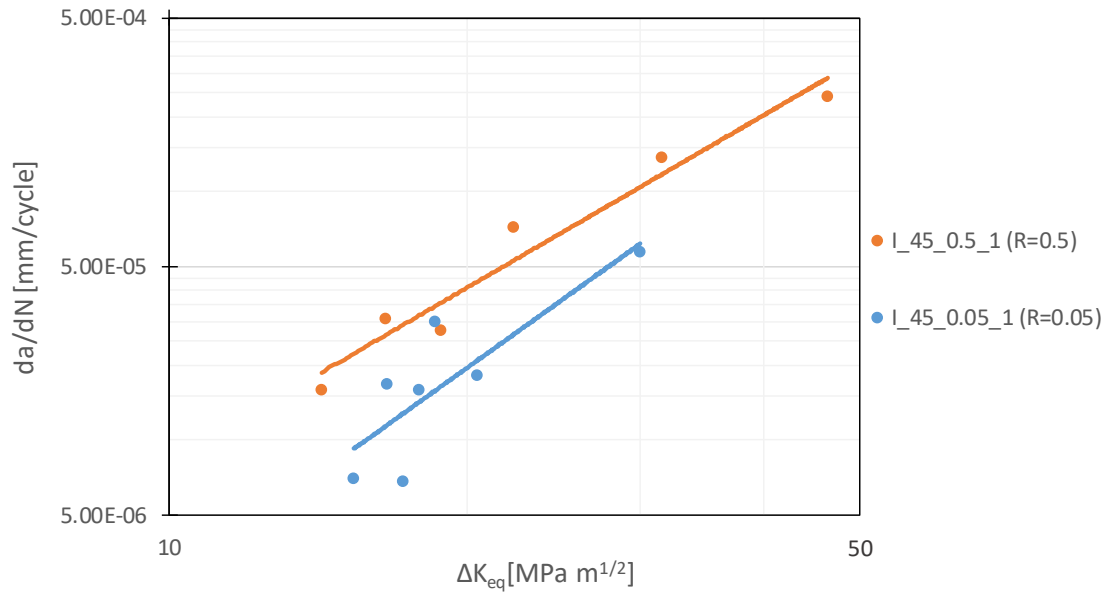


Figure 4.40 - Kinetic fatigue fracture diagram for $\alpha=45^\circ$ and non pre-strained specimens.

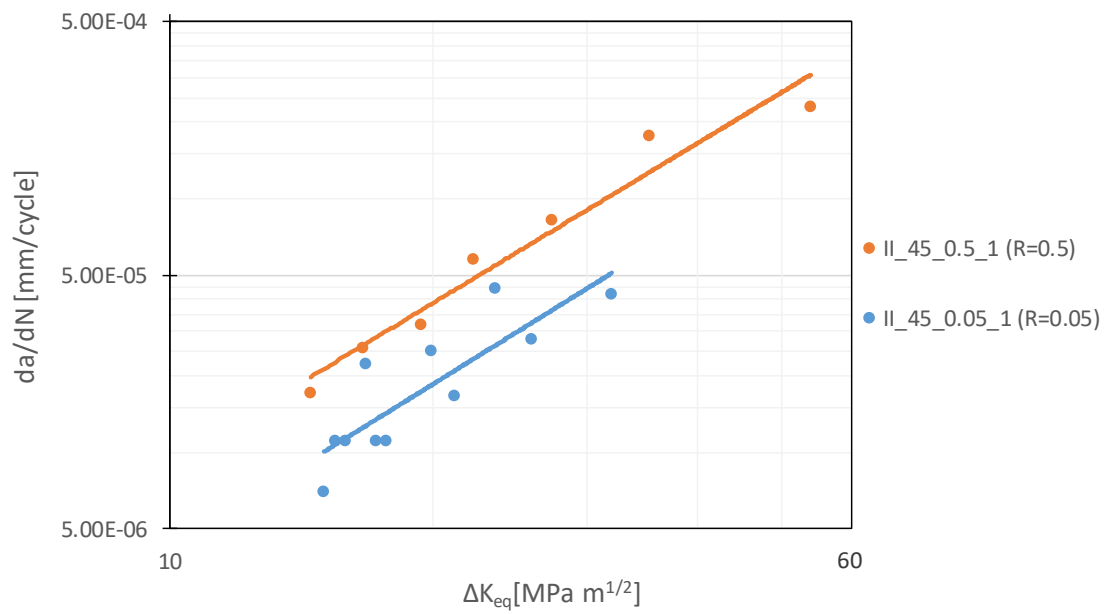


Figure 4.41 - Kinetic fatigue fracture diagram for $\alpha=45^\circ$ and pre-strained specimens.

▪ $\alpha = 60^\circ$

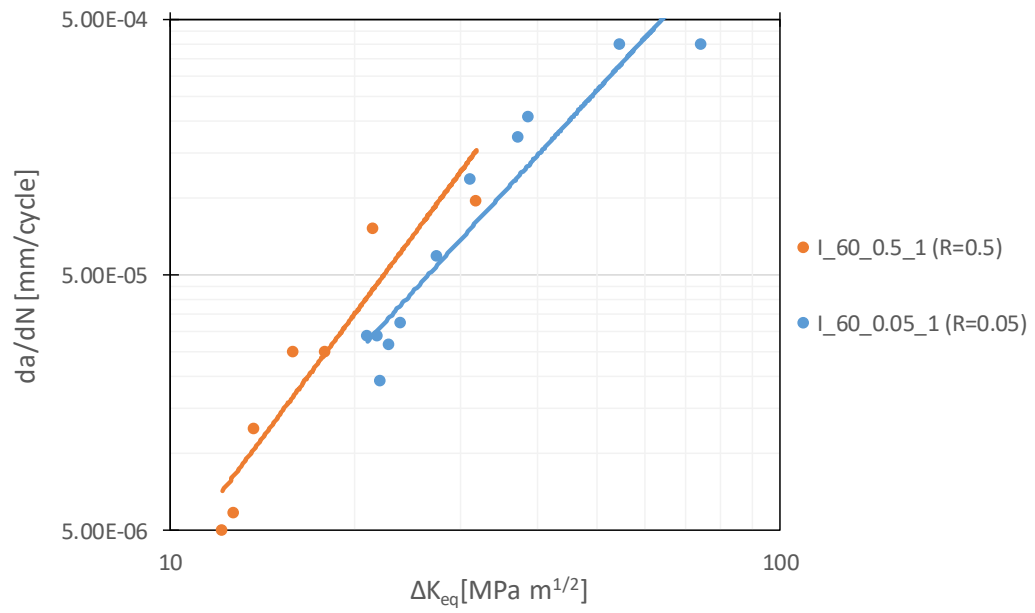


Figure 4.42 - Kinetic fatigue fracture diagram for $\alpha=60^\circ$ and non pre-strained specimens.

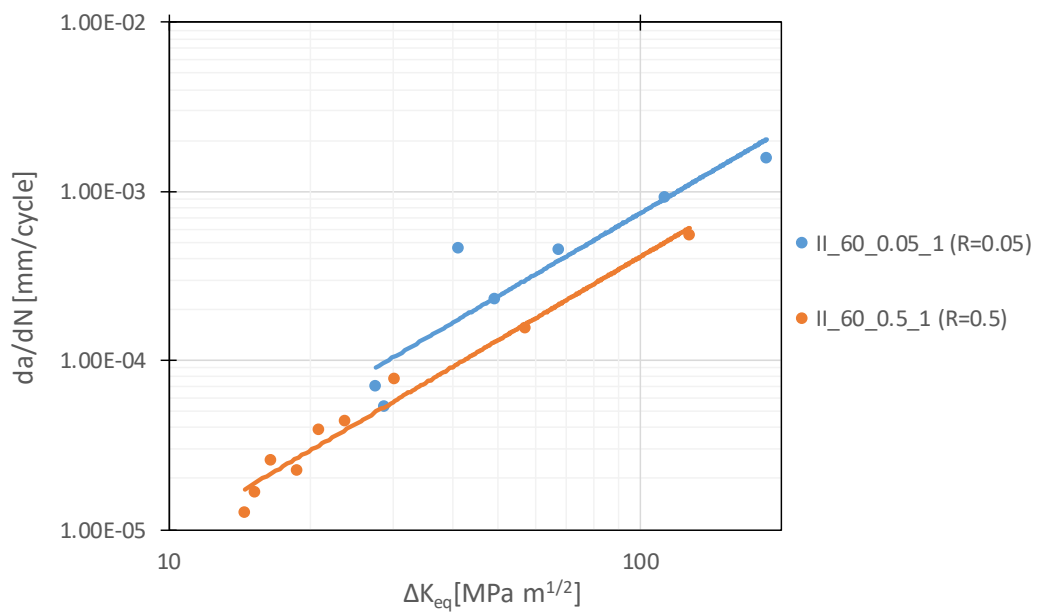


Figure 4.43 - Kinetic fatigue fracture diagram for $\alpha=60^\circ$ and pre-strained specimens

As we can see from the results, the fatigue crack growth rate for the specimens loaded under $R = 0.5$ was higher when compared to the ones loaded under $R = 0.05$ condition, for almost all cases. Therefore, the increase in stress ratio causes an increase in fatigue crack growth rate, as expected. This effect was verified for the different mixed mode loading conditions tested. For the case of pre-strained specimens under a loading angle of 60° , the results were contrary. The difference of the stress ratio effect between pre-strained and non pre-strained specimens was not significant.

4.3.3 Influence of loading angle

▪ $R = 0.05$

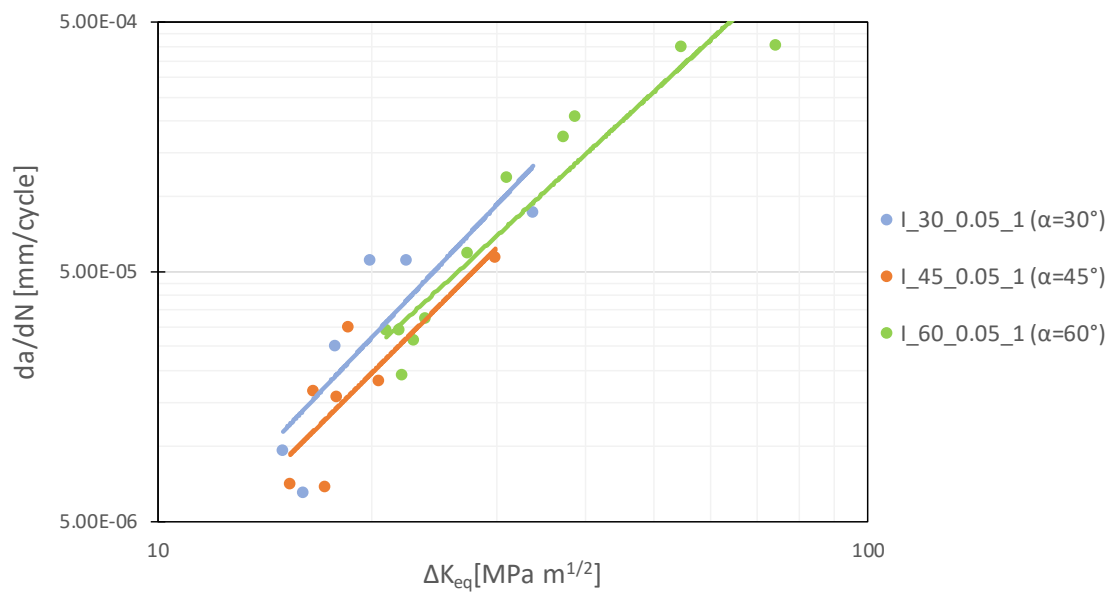


Figure 4.44 - Kinetic fatigue fracture diagram for $R=0.05$ and non pre-strained specimens

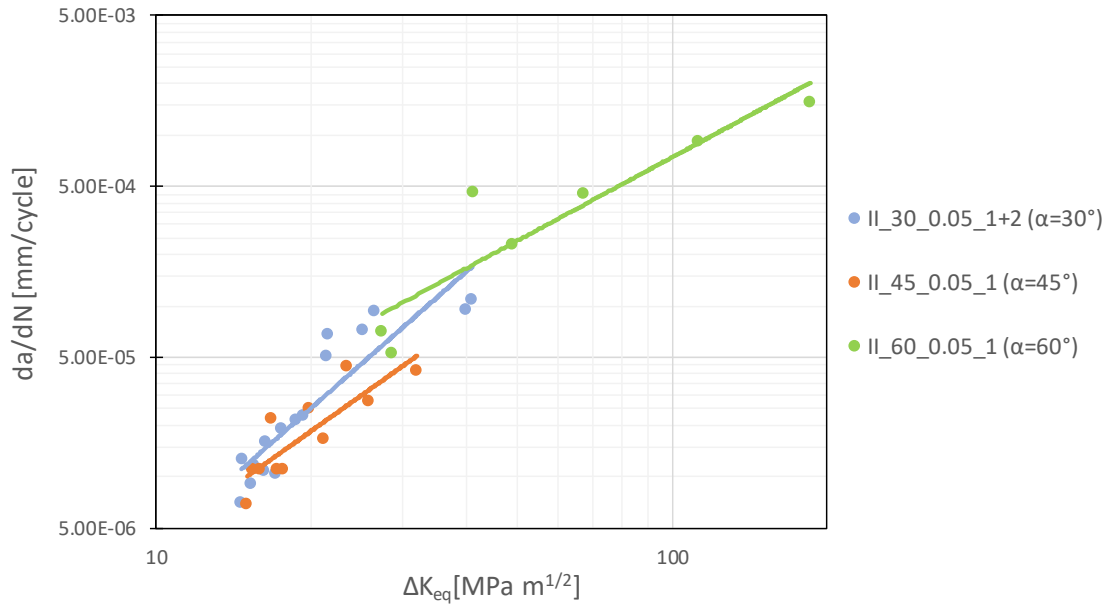


Figure 4.45 - Kinetic fatigue fracture diagram for R=0.05 and pre-strained specimens

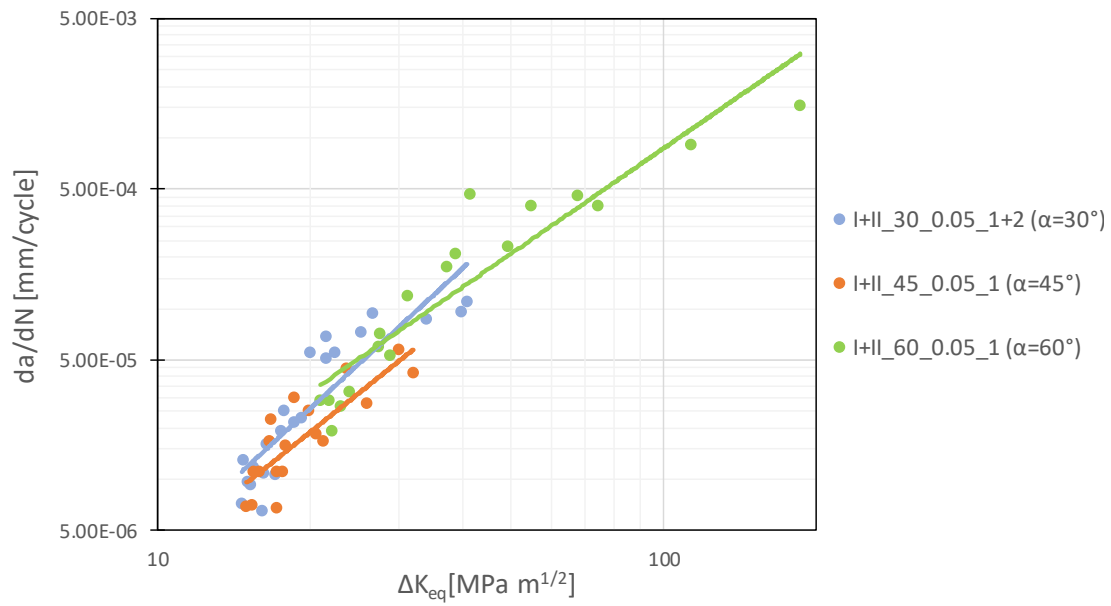


Figure 4.46 - Kinetic fatigue fracture diagram for R=0.05 (pre-strained + non pre-strained specimens)

▪ **R = 0.5**

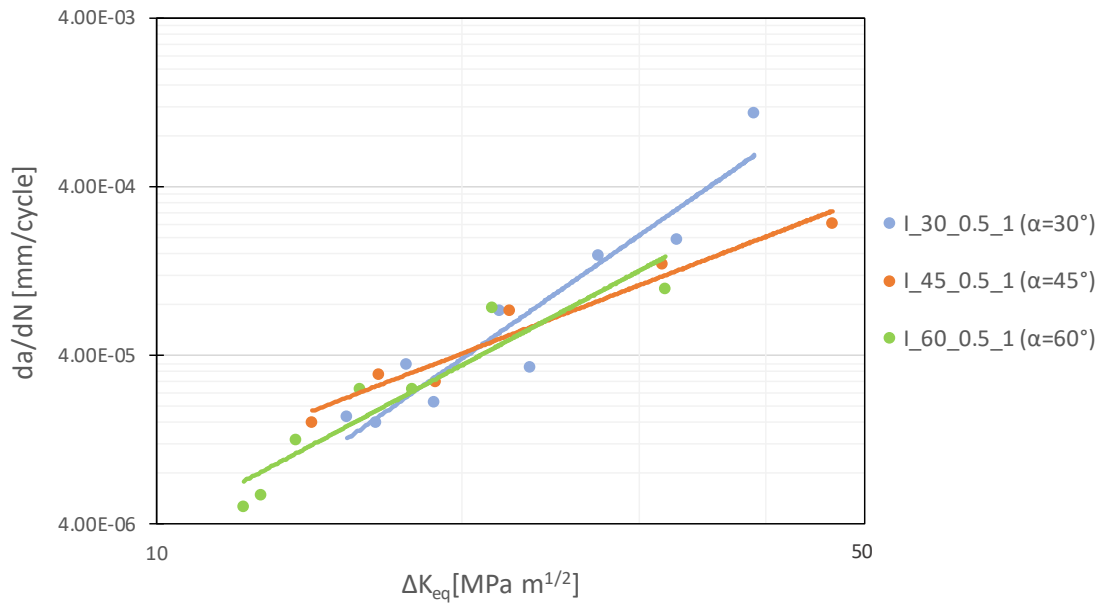


Figure 4.47 -Kinetic fatigue fracture diagram for R=0.5 and non pre-strained specimens

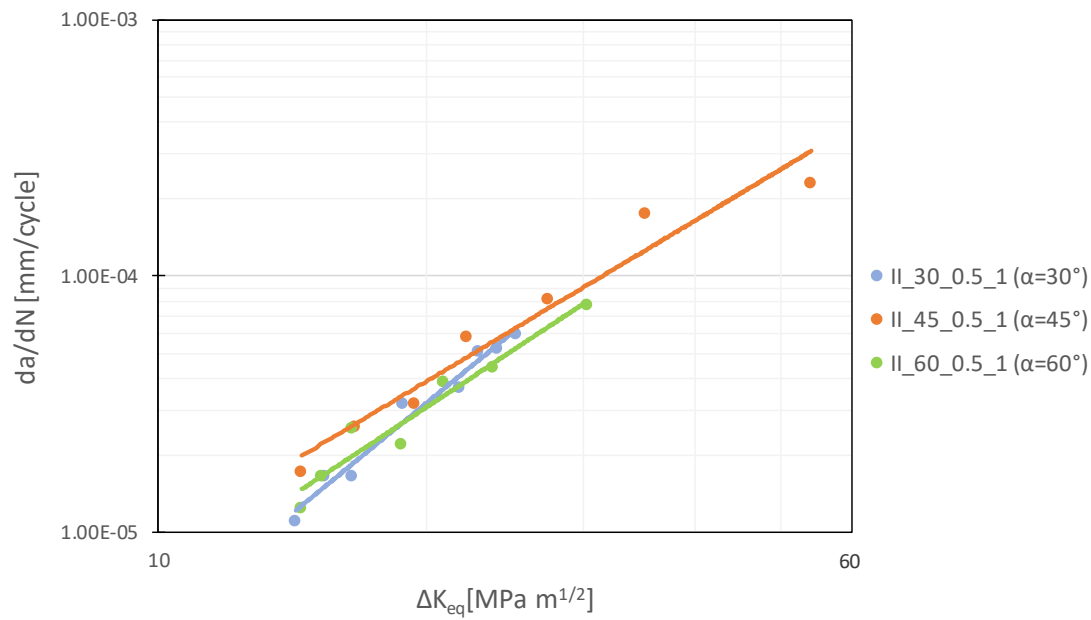


Figure 4.48 - Kinetic fatigue fracture diagram for R=0.5 and pre-strained specimens.

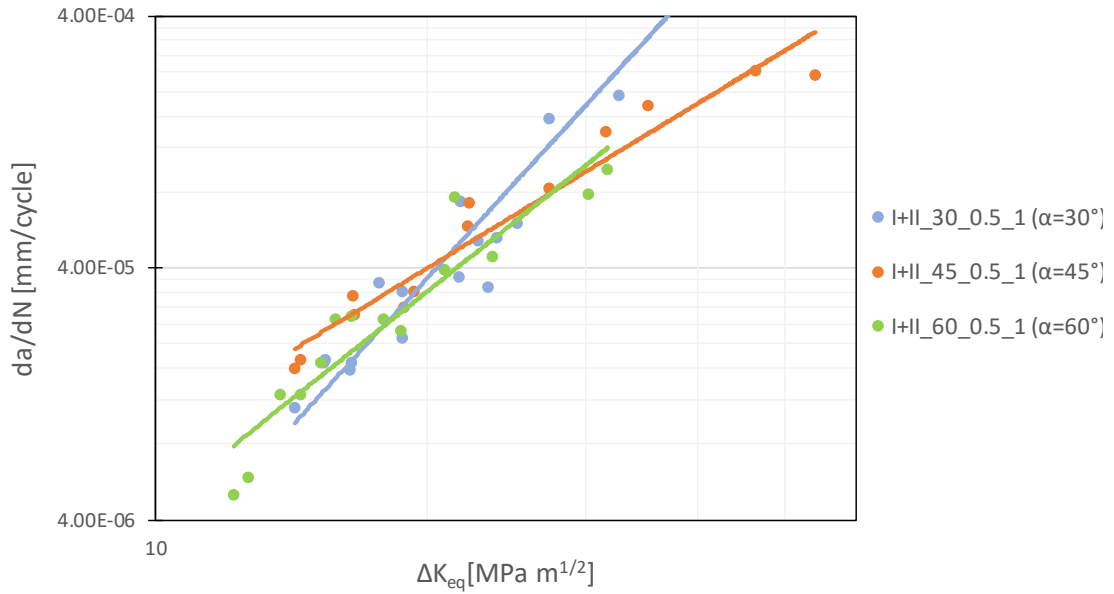


Figure 4.49 - Kinetic fatigue fracture diagram for $R=0.5$ (pre-strained + non pre-strained specimens).

Analysing Figure 4.44, Figure 4.45, Figure 4.46, Figure 4.47, Figure 4.48 and Figure 4.49 one can state that, for the region of high ΔK_{eq} , the crack propagates faster under the mixed mode loading condition of $\alpha = 30^\circ$. The lower fatigue crack propagation rates corresponded to $\alpha = 45^\circ$. This trend was verified for all conditions of stress ratio and pre-strain level.

For low ΔK_{eq} -values, the crack propagation behavior under different mixed mode conditions was more sensitive to the stress ratio. For $R = 0.05$, the $\alpha = 45^\circ$ condition presented the highest crack growth rates while the $\alpha = 60^\circ$ condition presented the lowest. For $R = 0.5$, the cracks grew faster for a loading angle of 30° and slower for a loading angle of 45° .

The degree of pre-strain has not significantly altered the effects of the different mixed mode loading conditions.

4.3.4 Paris Law constants

For each specimen, the experimental points were fitted to a curve using a power function. Figure 4.50 shows the fitting curves for all specimens tested.

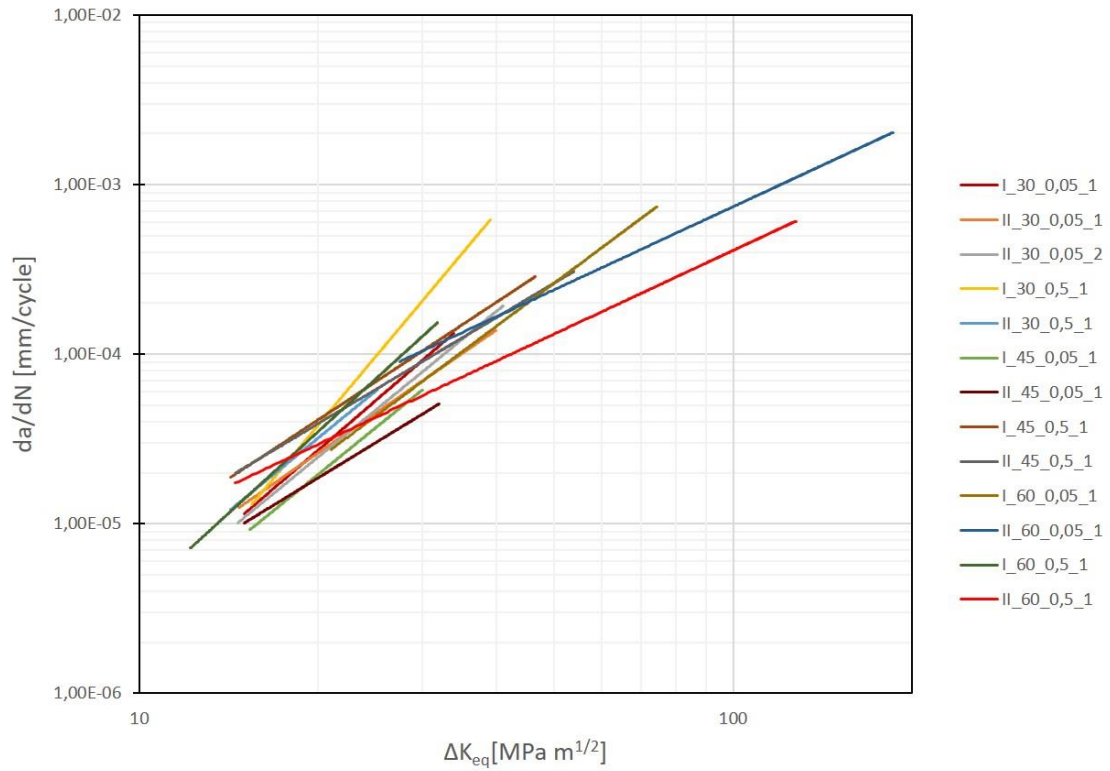


Figure 4.50 - Kinetic fatigue fracture diagrams of all specimens tested

Having determined the equations of the fitting curves, it was possible to obtain the Paris law constants. Table 4.5 presents the Paris law constants and the correlation coefficient for all specimens tested.

Table 4.5 -Paris law constants and correlation coefficient for all specimens

α	R	Pre-strain	Specimen	C	m	R^2
30°	0.05	0%	I_30_0.05_1	3.207E-09	3.018	0.72
		6%	II_30_0.05_1	1.866E-08	2.418	0.85
		6%	II_30_0.05_2	4.641E-09	2.864	0.81
		6%	II_30_0.05_1+2	8.988E-09	2.653	0.82
	0.5	0%	I_30_0.5_1	1.388E-10	4.179	0.90
		6%	II_30_0.5_1	5.990E-09	2.865	0.97
45°	0.05	0%	I_45_0.05_1	3.913E-09	2.842	0.69
		6%	II_45_0.05_1	3.044E-08	2.142	0.72
	0.5	0%	I_45_0.5_1	4.065E-08	2.309	0.95
		6%	II_45_0.5_1	7.492E-08	2.087	0.95
60°	0.05	0%	I_60_0.05_1	9.645E-09	2.610	0.90
		6%	II_60_0.05_1	4.126E-07	1.627	0.85
	0.5	0%	I_60_0.5_1	2.407E-09	3.198	0.88
		6%	II_60_0.5_1	2.188E-07	1.636	0.97

From the results displayed in Table 4.5, it is possible to conclude that, for a certain loading angle (α) and stress ratio (R), the pre-strain increased the coefficient C and decreased the exponent m . This means that the slope of da/dN vs. ΔK_{eq} curve is lower for the pre-strained specimens than for the non pre-strained ones. These results are in agreement with the investigations of Donato and Cavalcante (2015) and Netto *et al.* (2008).

Furthermore, the results reveal that, for a constant stress ratio (R) and pre-strain level, the coefficient C tends to increase with the loading angle, while the exponent m decreases.

For the loading angles of 30° and 60° , the increase in the stress ratio (R) caused a decrease of the coefficient C and an increase of the exponent m . For a 45° loading angle, this trend was reversed.

5 Strain energy density models

Fatigue crack initiation, usually modelled by strain-life or stress-life, has traditionally been considered to be a separate physical phenomenon from fatigue crack propagation. However, some more recent models of fatigue crack growth have been based on the assumption that each crack growth increment is physically similar to initiation. That is, that individual crack growth increments are successive initiations of the crack locally at the crack tip (Glinka 1985, Huffman 2016, Noroozi *et al.* 2005, Pecker and Niemi 1999). Such models consider fatigue crack initiation and propagation to be physically similar, and they can be used in a unified approach to calculate total fatigue life as the sum of initiation and propagation (Hurley and Evans 2007, Noroozi *et al.* 2007, 2008).

Some authors, as Glinka (1985), Pecker and Niemi (1999), Noroozi *et al.* (2005, 2007, 2008), Hurley and Evans (2007) developed approaches to represent fatigue crack propagation using local fatigue models based on fatigue strain parameters. Glinka (1985) was one of the precursors to describe the fatigue crack propagation modelling using a strain-based fatigue relation.

Similarly, the model proposed by Pecker and Niemi (1999) allowed the near threshold fatigue crack propagation data and the stable crack growth to be described. For the near threshold fatigue crack propagation, the authors derived the following analytical relations function of the strain-life constants.

Hurley and Evans (2007) proposed the use of elastoplastic finite element analysis to compute the process zone using the Walker-like strain that was correlated directly with the fatigue life from the experimental data using a power relation.

Other authors, such as, Correia *et al.* (2012, 2013, 2014, 2015, 2013, 2016, 2017) and Hafezi *et al.* (2012) used the strain and SWT fatigue local relations (Smith *et al.* 1970) based on UniGrow model (Noroozi *et al.* 2005, 2007, 2008) to predict the fatigue crack propagation using the numerical analysis to obtain residual stresses distribution. Correia *et al.* (2013, 2015, 2017) proposed a procedure to derive probabilistic S–N–R fields for notched structural details or mechanical components, which is based on the UniGrow model and numerical analysis with aims to compute the elastoplastic stresses and strains. Alternatively, analytical methods such as

the ones proposed by Neuber (1961) and Moftakhar *et al.* (1994) may be applied to perform the elastoplastic analysis taking into account the elastic stress/strain fields computed around the crack tip, using available Linear Elastic Fracture Mechanics solutions (Moftakhar *et al.* 1994, Reinhardt *et al.* 1997, Noroozi *et al.* 2005).

Recently, Huffman (2016) suggested new developments related with the fatigue crack propagation modelling using strain energy density-based model. Fatigue evaluation of notched details based on unified local probabilistic approaches was also proposed by Huffman *et al.* (2017) considering the Walker-like strain-life relation in conjunction with the probabilistic model proposed by Castillo and Fernandez-Canteli (2009).

In this section, the Huffman crack propagation model is applied to the pressure vessel P355NL1 steel and a comparison with experimental is made.

5.1 Overview of local stress/strain approaches

Fatigue crack growth behaviour, like that which is typically characterized by the Paris law, uses Linear-Elastic Fracture Mechanics (LEFM) to describe loads and geometries and simultaneously. Local approaches, however, explicitly consider stresses and strains near the crack tip, and associate those with the stress intensity factor. Using the assumption that the stresses or strains near the crack tip are related to fatigue crack growth in the same way that global stresses and strains are related to stress-life and strain-life, an association can be made between damage parameters, such as the SWT damage parameter (Smith *et al.* 1970) or the damage parameter introduced by Huffman (2016).

The latter approach assumes that stress-life, strain-life, and fatigue crack growth are all different expressions of the same phenomenon which is directly associated with the cyclic stress-strain behaviour. According to that work, the stress life, strain life, and fatigue crack growth rate can all be expressed in terms of material parameters. The damage parameter in this case is:

$$\left(\frac{U_e}{U_d \rho_c}\right) \left(\frac{U_p^*}{U_d \rho_c}\right) = D = \frac{2N}{2N_f} \quad (5.1)$$

where U_e is the elastic strain energy density and U_p^* is the complimentary plastic strain energy density, both from integrating the cyclic Ramberg-Osgood stress-strain curve (Ramberg and Osgood 1943), ρ_c is the critical dislocation density, U_d is the dislocation strain energy, and D is the damage done for those strain energy inputs. The dislocation strain energy is calculated as:

$$U_d = \left(\frac{E}{2(1+\nu)} \right) |\vec{b}|^2 \quad (5.2)$$

Where ν is the Poisson's ratio and $\vec{b}$ is the Burger's vector. The stress based damage is then

$$\left(\frac{2}{U_d^2 \rho_c^2} \right) \left(\frac{n'}{1+n'} \right) \left(\frac{1}{K'} \right) \sigma_{max}^2 (\sigma_a)^{\frac{1+n'}{n'}} = D = \frac{1}{2N_f} \quad (5.3)$$

The Morrow constants can be calculated as follows.

$$\sigma_f' = E \left(\frac{2}{U_d^2 \rho_c^2} \left(\frac{1}{K'} \right)^{\frac{1}{n'}} \left(\frac{n'}{1+n'} \right) E^{(1+2n')n'} \right)^{-\frac{n'}{1+3n'}} \quad (5.4)$$

where E is the elastic modulus, K' is the cyclic strength coefficient, n' is the cyclic strain hardening exponent, and σ_f' is the fatigue strength coefficient. In this model, K' and n' come from fitting the cyclic stress and strain amplitude to the Ramberg Osgood equation.

$$b = -\frac{n'}{1+3n'} \quad (5.5)$$

where b is the fatigue strength exponent.

$$\varepsilon_f' = E \left(\frac{2(K')^3}{U_d^2 \rho_c^2 E} \left(\frac{n'}{1+n'} \right) \right)^{-\frac{1}{1+3n'}} \quad (5.6)$$

where ε_f' is the fatigue ductility coefficient

$$c = -\frac{1}{1+3n'} \quad (5.7)$$

where c is the fatigue ductility exponent.

Multiplying Equation (5.3) by Δa , gives a method for calculating fatigue crack growth rates.

$$\left(\frac{\Delta a}{U_d^2 \rho_c^2} \right) \left(\frac{n'}{1+n'} \right) \left(\frac{1}{K'} \right) \sigma_{max}^2 (\sigma_a)^{\frac{1+n'}{n'}} = \frac{\Delta a}{N_f - 0} \approx \frac{da}{dN} \quad (5.8)$$

where the stresses are the local stresses at the crack tip. These can be related to the stress intensity factor to yield a formula similar to what is used by (Noroozi *et al.* 2005, 2007, 2008) or alternatively to use the procedure to obtain the fatigue crack growth driving force, $\Delta \kappa$, considering a finite element analysis to compute residual stress intensity factor, K_r , proposed by Correia *et al.* (2012, 2013, 2014, 2015, 2013, 2016, 2017) and Hafezi *et al.* (2012),

$$\frac{da}{dN} = C(\Delta \kappa)^\gamma \quad (5.9)$$

where C is the fatigue crack growth rate coefficient, γ is the fatigue crack growth rate exponent, and $\Delta \kappa$, the fatigue crack growth driving force.

$$\begin{aligned}\Delta\kappa &= K_{max}^p(\Delta K)^{1-p} \\ K_{max} &= K_{max,applied} + K_r \\ \Delta K &= \Delta K_{applied} + K_r\end{aligned}\tag{5.10}$$

Where, ΔK total stress intensity factor range, ΔK total minimum stress intensity factor, $\Delta K_{applied}$ is the applied stress intensity factor range, $K_{max,applied}$ is the maximum applied stress intensity factor, and the p parameter is given by,

$$p = \frac{2n'}{1 + 3n'}\tag{5.11}$$

The fatigue crack growth rate exponent can be shown to be

$$\gamma = \frac{2 + 6n'}{1 + n'} = -\frac{2}{b + c}\tag{5.12}$$

For predominantly plastic stresses at the crack tip, the fatigue crack growth rate coefficient under the same conditions is given by,

$$C = \left(\frac{\Delta a}{U_d^2 \rho_c^2} \right) \left(\frac{n'}{1 + n'} \right) (K')^{\frac{2}{1+n'}} (E)^{-\frac{4n'+2}{n'+1}} \left(\frac{1}{2} \right)^{\frac{1}{n'}} \left(\frac{1}{2\pi x} \right)^{\frac{1+3n'}{1+n'}}\tag{5.13}$$

5.2 Application of the strain energy density approach to FCG data

Experimental fatigue crack growth data

The P355NL1 steel is a pressure vessel steel and was selected to apply the strain energy density approach to fatigue crack growth data. The mechanical properties of this steel was collected from the references (Correia *et al.* 2013, De Jesus and Correia 2013, De Jesus *et al.* 2007, Correia, De Jesus, Moreira, and Tavares 2016). In this sub-section is presented the monotonic strength data, cyclic elastoplastic fatigue data and the fatigue crack growth data obtained for the P355NL1 steel (Correia *et al.* 2013, De Jesus and Correia 2013, De Jesus *et al.* 2007, Correia, De Jesus, Moreira, and Tavares 2016).

The elastic and monotonic tensile properties, this steel under investigation, as represented by the Young modulus, E , Poisson ratio, ν , the ultimate tensile strength, f_u , upper yield stress, f_y , monotonic strain hardening coefficient, K , and monotonic strain exponent, n , are shown in Table 5.1. Also, in Table 5.1, the Ramberg-Osgood properties are represented by the cyclic strain hardening coefficient, K' , and the cyclic strain exponent, n' .

The strain-life behaviour of the P355NL1 steel was evaluated through fatigue tests of smooth specimens, carried out under strain-controlled conditions, according to the ASTM E606 standard (ASTM 1998). Two series of specimens are tested under distinct strain ratios, $R_\epsilon=0$ and -1 , 19 and 24 specimens, respectively. The cyclic Ramberg-Osgood (Ramberg and Osgood 1943) and Morrow (Coffin Jr 1954, Manson 1954, Morrow 1965) strain-life parameters of the P355NL1 steel are summarized in Table 5.2, for the conjunction of both strain ratios. The Morrow strain-life parameters, such as, the fatigue ductility coefficient, ϵ_f' , the fatigue ductility exponent, c , the fatigue strength coefficient, σ_f' , and the fatigue strength exponent, b , were collected in references (Correia *et al.* 2013, De Jesus and Correia 2013, De Jesus *et al.* 2007, Correia, De Jesus, Moreira, and Tavares 2016).

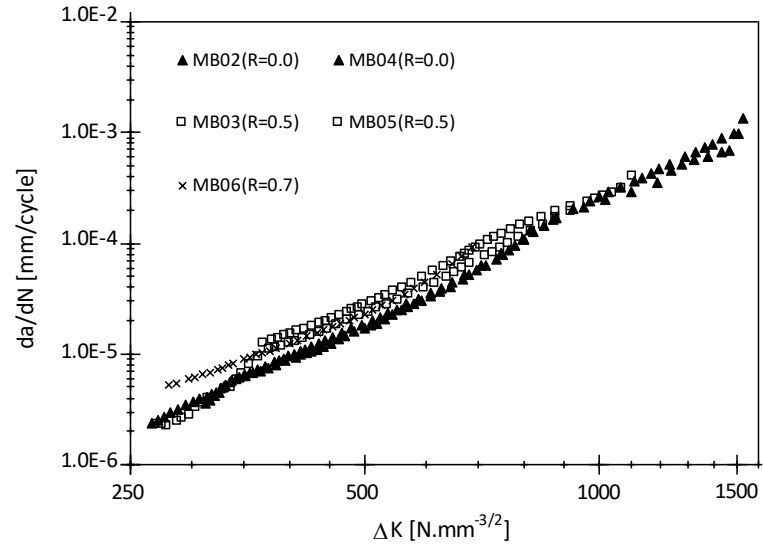
Table 5.1 - Elastic, monotonic and Ramberg-Osgood tensile properties for the P355NL1 steel

E (GPa)	ν	f_y (MPa)	f_u (MPa)	K (MPa)	n	K' (MPa)	n'
205.0	0.275	361.99	361.99	611.49	0.063	948.35	0.1533

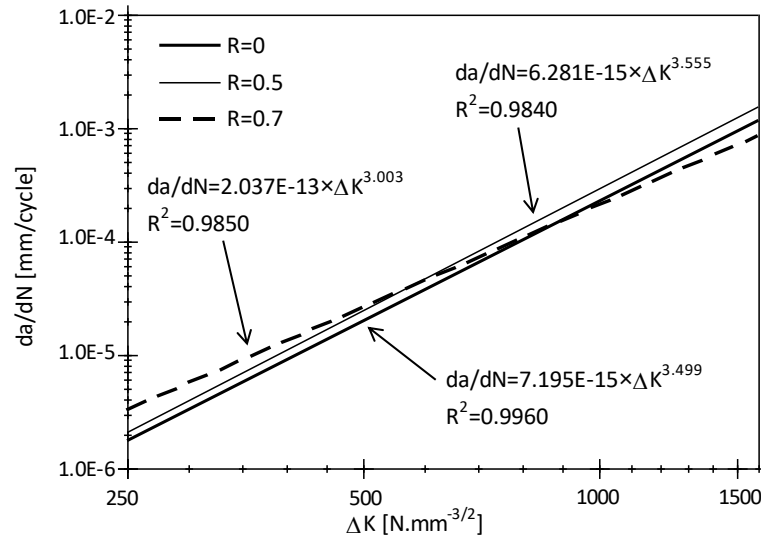
Table 5.2 - Morrow constants for the P355NL1 steel obtained using the Coffin-Manson equation for strain R -ratios, $R_\epsilon=-1 + R_\epsilon=0$

σ_f' (MPa)	b	ϵ_f'	c
1005.50	-0.1033	0.3678	-0.5475

The experimental results of the fatigue crack growth rates of the investigated material are also evaluated for several stress R -ratios, using CT specimens, following the recommendations of the ASTM E647 standard (ASTM 2000). The CT specimens of P355NL1 steel are defined with a width, $W=40\text{mm}$ and a thickness, $B=4.5\text{mm}$ (Correia *et al.* 2013, De Jesus and Correia 2013, De Jesus *et al.* 2007, Correia, De Jesus, Moreira, and Tavares 2016). The tests were executed in air, at room temperature, under a sinusoidal waveform at a maximum frequency of 20 Hz. In Figure 5.1, the crack growth data derived for the P355NL1 steel, for three tested stress ratios, $R_\sigma=0.0$, $R_\sigma=0.5$ and $R_\sigma=0.7$., are shown. The crack propagation rates are only slightly influenced by the stress ratio. Higher stress ratios provide higher crack growth rates (Correia *et al.* 2013, De Jesus and Correia 2013, De Jesus *et al.* 2007, Correia, De Jesus, Moreira, and Tavares 2016)



a)



b)

Figure 5.1 - Fatigue crack growth data of the P355NL1 steel: a) Experimental data; b) Paris correlations for each stress R-ratio

Brief description of the numerical analysis

In this sub-section, a brief description of the numerical analysis of the CT specimens performed by Correia *et al.* (2013, 2014, 2016) is presented. This author developed two dimensional numerical models of the CT specimens using non-linear elastoplastic finite element modelling. These models were useful to assess the accuracy of residual stress estimation using the simplified elastoplastic analysis. Figure 5.2 illustrates the finite element mesh of the CT specimen along with the respective boundary conditions. One half of the geometry is modelled,

taking advantage of the existing symmetry. 2D plane stress elements are used since the specimens' thickness is relatively thin ($B=4.5\text{mm}$ for the P355NL1 steel). Quadratic triangular elements (6-noded elements) are selected and applied with a full integration formulation. The pin loading used in the CT testing is simulated with a rigid-to-flexible frictionless contact, the pin being modelled as a rigid circle controlled by a pilot node. All numerical simulations are carried out using the ANSYS® 12.0 code (ANSYS 2011). The 6-noded plane element adopted in the FE (finites elements) analyses is the PLANE181 element available in the ANSYS® 12.0 code library. The contact and target elements used in the pin-loading simulation are, respectively, the CONTA172 and TARGE169 elements available in ANSYS® 12.0 code (ANSYS 2011). A parametric model is built using the APDL language. The surface of the holes is modelled as flexible, using CONTA172 elements. The Augmented Lagrange contact algorithm is used. The associative Von Mises (J2) yield criterion with multilinear kinematic hardening is used to model the plastic behaviour. The multilinear kinematic hardening uses the Besseling model, also called the sublayer or overlay model, so that the Bauschinger effect is included. The plasticity model was fitted to the stabilized or half-life pseudo stabilized cyclic curve of the materials. The von Mises yield criterion with multilinear kinematic hardening is adopted to model the plastic behaviour. The plasticity model is fitted to the cyclic curve of the material using the cyclic Ramberg-Osgood properties of the Table 5.1.

The finite element model is applied to perform elastic and elastoplastic stress analyses. One important assumption of the Huffman fatigue crack propagation model consists in applying the compressive residual stresses that are computed ahead of the crack tip, in the crack faces, in a symmetric way with respect to the normal to crack face that passes through the crack tip. The resulting residual stress intensity factor, K_r , is computed using the weight function method (Glinka 1996), for each of the stress ratios covered by the testing program.

Figure 5.3 presents the residual stress intensity factor range (De Jesus and Correia 2013) as a function of the applied stress intensity factor range, obtained with the numerical analysis.

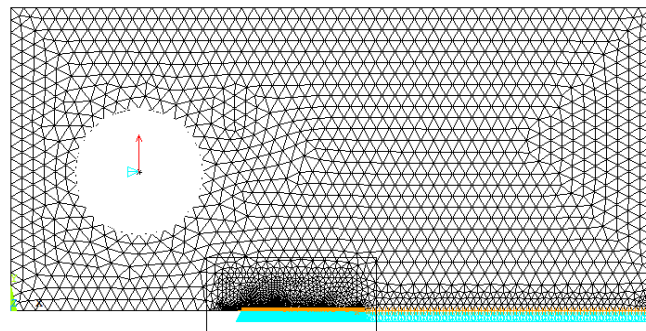


Figure 5.2 - Finite element mesh of the CT specimen (Correia et al. 2013)

Therefore, the numerical solution, for the residual stresses, is adopted in the crack propagation prediction, based on the UniGrow model, which is also important to apply the Huffman fatigue crack propagation model (Huffman 2016). A very high linear correlation between the residual stress intensity factor and the applied stress range is verified, for each stress R -ratio. This linear relation agrees with the proposition by Noroozi *et al.* (2005), based on analytical analysis.

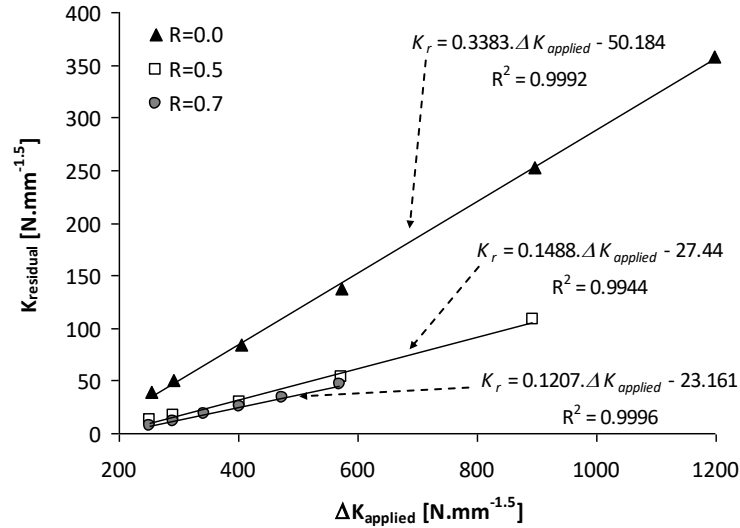


Figure 5.3 - Residual stress intensity factor as a function of the ΔK applied for the CT geometry

Application and results

This sub-section presents the application of the strain energy density approach to fatigue crack propagation data of the P355NL1 pressure vessel steel. From the application of this model, the Morrow and fatigue crack growth constants, as well as, the strain-life and stress-life curves are evaluated. The fatigue crack propagation model based on the strain energy density approach requires the calibration of model parameters, such as, the critical dislocation density, ρ_c , crack increment, Δa , and fatigue crack growth rate constant, x .

The Morrow constants resulting from Equations (5.4)-(5.7) can be seen in Table 5.3. These constants, estimated using the strain energy density approach, are similar to those obtained by fitting the Coffin-Manson and Morrow relation. In this way, it is demonstrated that the Huffman fatigue crack propagation model leads to good results. The fatigue crack growth rate constants are shown in Table 5.4, as well as the critical dislocation density for the material. These FCG rate constants were estimated using the strain energy density approach related to the stress intensity factor used by Noroozi *et al.* (2005) and considering the residual stresses distribution obtained numerically (proposed by Correia *et al.* (2013)).

Table 5.3 - Morrow constants for the P355NL1 calculated as per Equations (5.4)-(5.7)

σ'_f (MPa)	b	ε'_f	c
959	-0.105	1.08	-0.685

Table 5.4 - Fatigue crack growth rate constants from Equation (5.8)

ρ_c (m/m ³)	Δa (m)	x (m)
7.0x10¹⁵	4.5E-3	3.0E-5

The stress-life and strain-life curves, and fatigue crack growth rates calculated from the strain energy based damage model are presented, respectively, in Figure 5.4, Figure 5.5 and Figure 5.6. A good agreement between the experimental strain-life tests results and stress- and strain-life curves estimated using the strain energy density approach. These results were obtained using the Burger's vector modulus equal to 2.52E-10, which is currently used for structural steels (Huffman 2016). A good agreement can be considered between the experimental FCG results and the results obtained on the basis of the Huffman fatigue crack propagation model for the stress R -ratio equal to 0 and the set of experimental FCG results for all stress R -ratios. This model shows to be very conservative for the stress R -ratios equal to 0.5 and 0.7, describing a possible mean stress effect that is not verified for this material. The critical dislocation density and basic fatigue crack growth rates constants (Table 5.4) for the P355NL1 steel are similar when compared with other materials of identical mechanical properties (see ref. Huffman (2016))

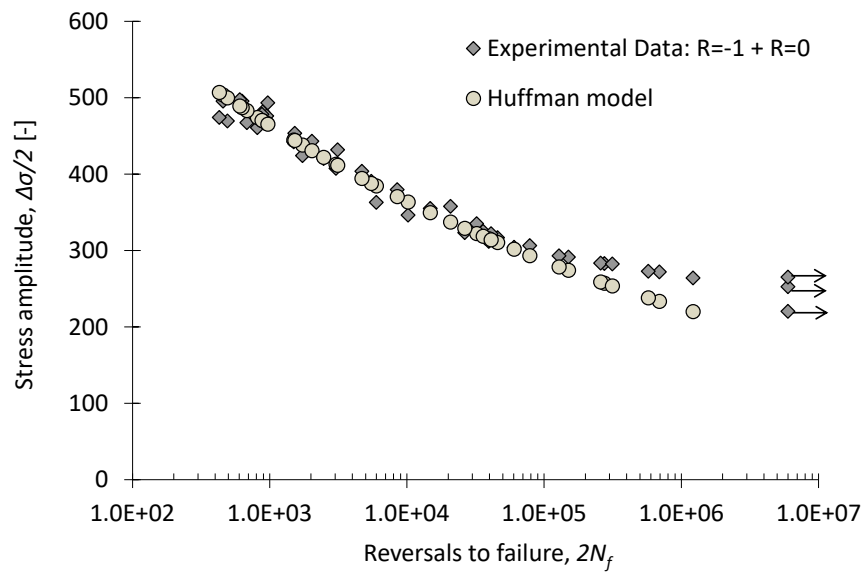


Figure 5.4 - Stress-life curve of the P355NL1 steel, per Huffman (2016)

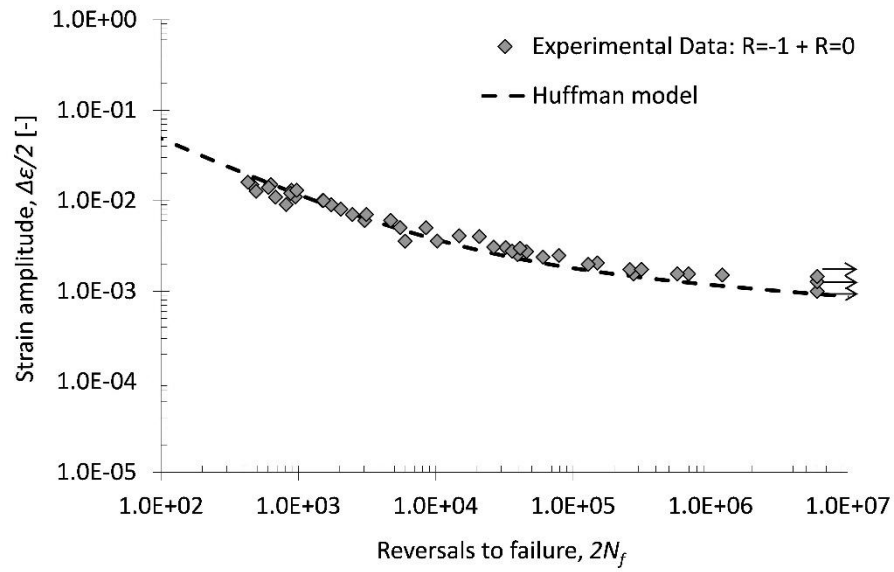
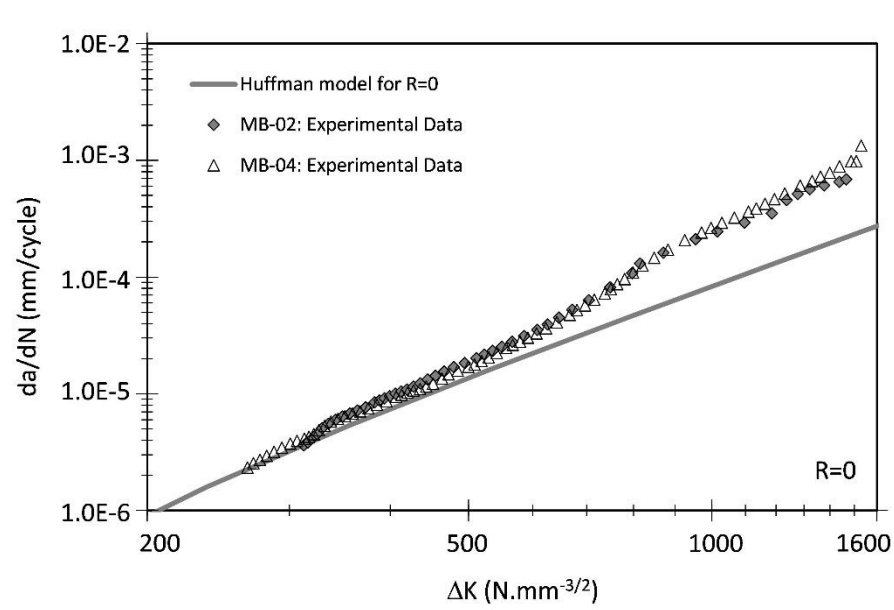
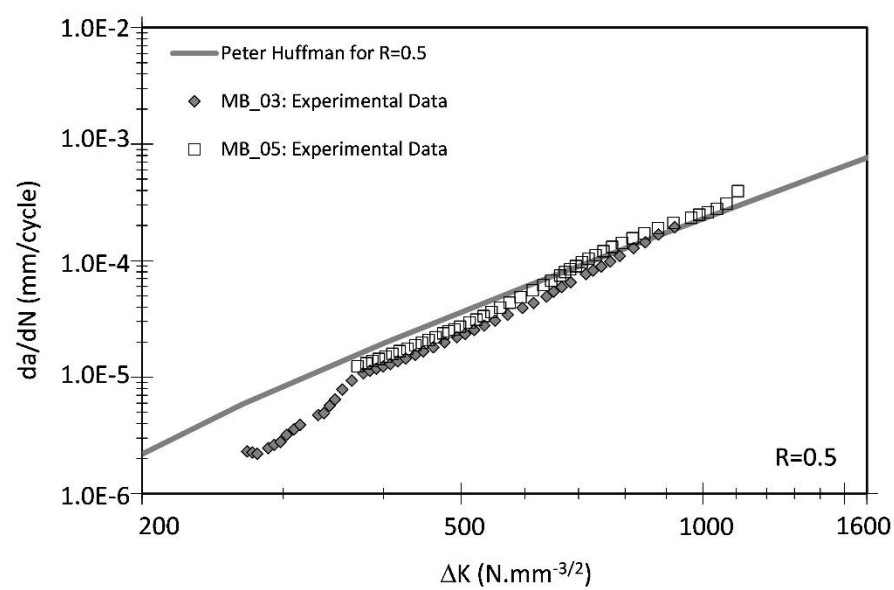


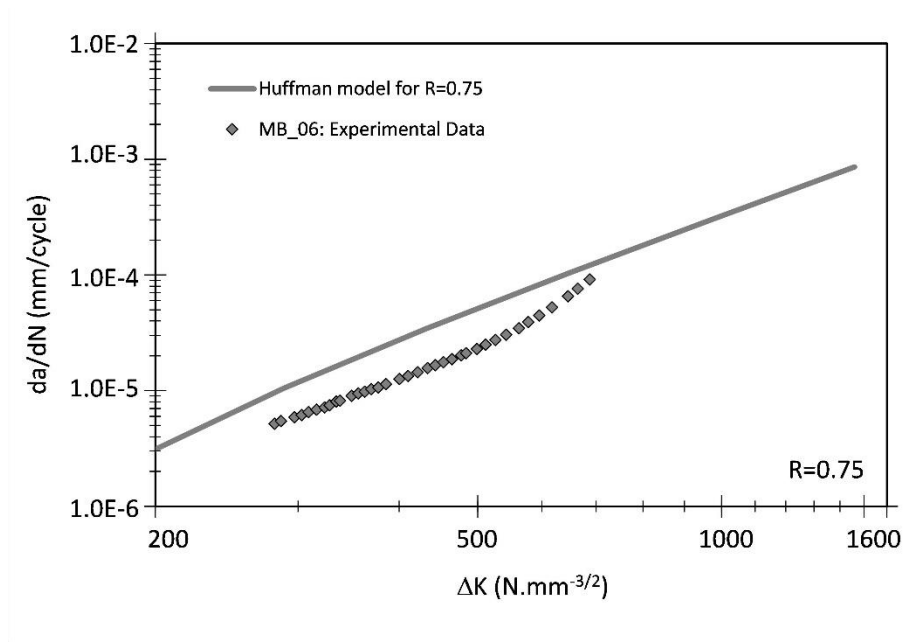
Figure 5.5 - Strain-life curve of the P355NL1 steel, per Huffman (2016)



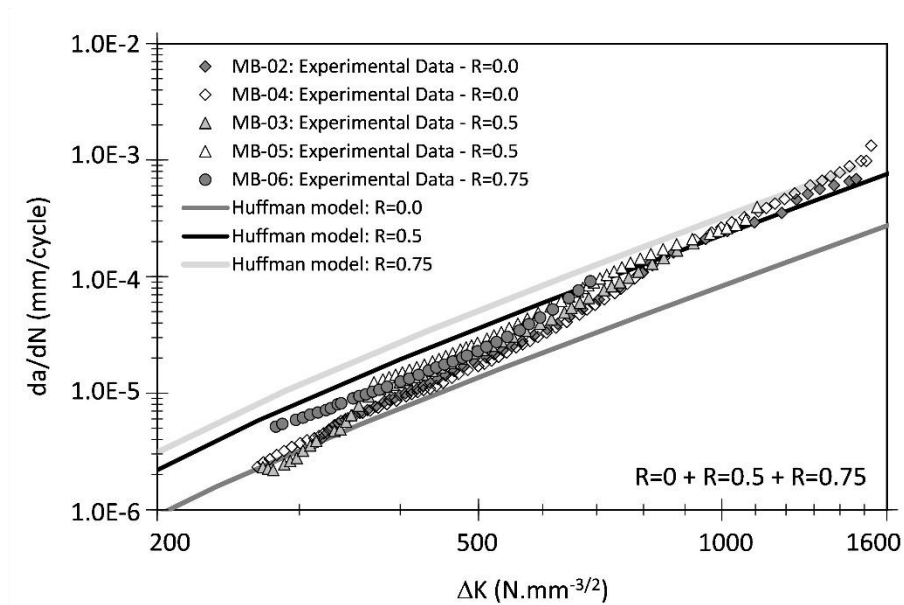
a)



b)



c)



d)

Figure 5.6 - Fatigue crack growth rate of P355NL1 steel obtained using the Huffman model, as per Huffman (2016): a) $R=0$; b) $R=0.5$; c) $R=0.75$; d) $R=0 + R=0.5 + R=0.75$

5.3 Conclusions

The Huffman fatigue crack initiation and propagation model based on a strain energy based damage model can be used to predict the stress-life and strain-life curves, as well as the fatigue crack propagation rates in Paris' law regime, along with the corresponding stress ratio effect.

A good agreement between the experimental strain-life results and Huffman analytical stress- and strain-life curves is verified. The model is able to obtain fatigue local relations for other fatigue damage parameters, such as SWT fatigue damage parameter.

The application of the Huffman model to the fatigue crack propagation data of the P355NL1 steel proved to be promising. Some aspects related with the mean stresses and stress R -ratios effects have to be improved in order to correctly describe the fatigue crack growth behaviour of the material under consideration. A comparison with other models of fatigue crack propagation based on fatigue local approaches should be made, such as UniGrow model using Neuber analytical approaches or numerical modelling to obtain the residual stresses distribution.

A unified two-stage fatigue approach using the Huffman fatigue crack initiation and propagation model applied to the structural details can be suggested.

6 Conclusions and future research

6.1 Main conclusions

Through the experimental campaign carried out in the context of this work, it was possible to conclude that, for the same loading conditions (α and R), the pre-straining operation increased the fatigue crack initiation angle.

Another clear trend was the influence of pre-strain on the fatigue crack growth resistance of P355NL1 steel. The results revealed that the tensile pre-strain reduced fatigue crack growth rates in the Paris region, for all stress ratios and loading angles. The fatigue crack growth data in the low ΔK region indicated that pre-strain might decrease fatigue crack growth threshold (ΔK_{th}). It was corroborated that fatigue cracks propagate faster under higher stress ratios.

Moreover, it was found that the application of pre-strain has a clear impact on the values of the Paris law constants. Under the same loading conditions (R and α), pre-straining the specimens increases the coefficient C and decreases the exponent m . A similar influence was observed for increasing loading angles.

It was verified that the strain energy density model proposed by Huffman was able to predict satisfactorily strain-life curves, stress-life curves and Paris' region fatigue crack growth rates for the P355NL1 steel. However, further modifications in the Huffman model are needed in order to allow the correct description of mean stress and stress ratio influence on fatigue crack growth behavior of the material under consideration.

6.2 Future works

As a continuation of this work, it would be interesting to conduct additional fatigue crack growth tests for more pre-strain levels and angles to confirm the trends showed in this work. It is proposed the use of higher pre-strain levels (e.g. 9% and 12%) to investigate if it alters the crack growth behavior of the material.

The application of mixed mode fracture criteria (e.g. maximum tangential stress criterion) to predict the crack initiation angle and comparison with experimental results is suggested.

Another relevant work would be the investigation of crack closure effects on fatigue crack propagation in CTS specimens with different degrees of pre-strain.

Testing in the threshold region under mixed mode loading would also be important to clarify the effect of the pre-strain on the ΔK_{th} .

A comparison of the Huffman model with other models of fatigue crack propagation based on fatigue local approaches should be made, such as UniGrow model using Neuber analytical approaches or numerical modelling to obtain the residual stresses distribution.

Another proposal is the modification of the Huffman model in order to allow its application to crack propagation under mixed mode (I+II) loading.

Finally, a unified two-stage fatigue approach using the Huffman fatigue crack initiation and propagation model applied to the structural details can be suggested.

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