

Development and Validation of Industrial-Scale Predictive Models for Composite Bolted Joints

Rafael Cruz Valério

Supervisor: Prof^a. Dr^a. Carolina Furtado

Co-supervisor: Dr. Pedro Silva Campos

Co-supervisor: Dr. Denis Dalli

Dissertation for the degree of Master of Science in Mechanical Engineering for the
Faculty of Engineering of the University of Porto

July 5, 2026

Abstract

Composite bolted joints remain one of the most critical structural features in aerospace engineering due to their influence on structural integrity, damage tolerance, and maintenance requirements. While high-fidelity numerical models provide accurate predictions of joint behavior, their computational cost limits their applicability in large-scale industrial design and optimization workflows. Consequently, there is a growing need for simplified predictive methodologies capable of maintaining sufficient accuracy while significantly reducing computational effort.

One of the main focus of thesis is the extensive experimental characterization of composite bolted joints to evaluate and compare conventional Quad (QUAD) and innovative Double-Double (DD) laminate stacking, considering both quasi-isotropic and stiffness-tailored configurations. The experimental campaign encompassed bearing, pull-through, combined bearing/pull-through, and multi-bolt single-lap shear tests. Digital Image Correlation (DIC) was systematically employed to capture full-field displacement measurements, providing crucial insights into load transfer mechanisms and failure evolution. Based on these experimental findings, three-dimensional failure interaction envelopes were successfully established to characterize the complex coupling between longitudinal bearing, transverse bearing, and pull-through failure mechanisms.

Another key aspect is the use of low-fidelity numerical modeling techniques developed for predictive analysis in industrial applications. Simplified finite element models based on connector formulations were implemented in Abaqus and calibrated using three distinct methodologies: elastic stiffness calibration, average stiffness calibration, and multi-linear plasticity calibration. The predictive performance of these numerical approaches was rigorously validated against experimental load-displacement curves and DIC measurements for both single and multi-bolt configurations. Ultimately, this methodology offers a robust framework to integrate experimentally derived failure envelopes into industrial-scale simulations, facilitating efficient structural analysis and preliminary design optimization of composite multi-bolt assemblies.

Keywords: *Double-Double Laminates, Composite Bolted Joints, Digital Image Correlation, Connector Elements, Failure Characterization, Industrial-Scale Simulation.*

Acknowledgments

I would like to express my sincere gratitude to Prof^a. Dr^a. Carolina Furtado for allowing me to do this project. I am also deeply grateful to Dr. Pedro Silva Campos, Anna Volpi, and Dr. Denis Dalli for their guidance, support and sharing with me their knowledge. Especially Dr. Pedro Silva Campos, who guided me every step of the way, from the very first experimental tests to the final report revisions.

I am deeply grateful to my parents and brothers for their patience and continuous support.

The author acknowledges the financial support by national funds through the FCT/MCTES, under the project 2023.15692.PEX – JOIN3D - JOInt eNergy absorption with Double-Double Design, with

DOI:<https://porto-composites.slack.com/archives/C0AK3CZH9PB/p1783209749377059>

Contents

Abstract	i
Acknowledgements	iii
List of Figures	vii
List of Tables	xi
Document Notation	xiii
1 Introduction	1
1.1 Motivation	1
1.2 Document layout	2
2 State-of-art and literature review	3
2.1 Composite materials	3
2.2 QUAD and Double-Double laminates	3
2.2.1 Equivalent Stiffness Search	4
2.2.1.1 Hard Laminates (High 0° Content)	4
2.2.1.2 Soft Laminates (High $\pm 45^\circ$ or 90° Content)	5
2.2.2 Manufacturing and Structural Efficiency	5
2.2.3 Optimization and Performance	5
2.2.4 Experimental Characterization and Mechanical Behavior	6
2.3 Mechanically-fastened joints and Notch Sensitivity Characterization	7
2.3.1 BEA – Bearing Strength Test	7
2.3.2 FHT – Filled Hole Tension	8
2.3.3 OHT – Open Hole Tension	8
2.3.4 OHC – Open Hole Compression	8
2.3.5 FHC – Filled Hole Compression	8
2.4 Low Fidelity models	8
2.4.1 Introduction and Computational Efficiency Imperatives	8
2.4.2 Evolution of Simplified Connector and Cartesian Frameworks	9
2.4.3 Macroscale Failure Envelope and Simplified Fastener Prediction	9
3 Experimental Test	11
3.1 Material Selection	11
3.1.1 Laminate Manufacturing	13
3.2 Pure Bearing Test	14
3.3 Pull-Through Test	17

3.4	Bearing/Pull-Through	19
3.4.1	Mechanical Components and Assembly	20
3.5	Single-Lap Shear Bolted Joint Testing	22
3.5.1	Experimental Methodology and Setup	22
3.5.2	Comparison between Failure Mechanisms	24
3.5.3	Comparison between stacking sequences	27
3.5.4	Comparison Between Joint Configurations	29
3.5.5	Digital Image Correlation Analysis	33
3.6	Failure Envelope Generation	37
3.6.1	Concluding Remarks	39
4	Computational Simulations	41
4.1	Simplified Connector Models	41
4.1.1	Numerical Modeling via Cartesian Connector Elements	42
4.1.1.1	Kinematic Constraints and Degree of Freedom Decoupling	42
4.1.1.2	Constitutive Behavior: Elasticity and Plasticity	43
4.1.2	Pure Bearing	44
4.1.3	Pure Pull-Through	45
4.1.4	Single-Lap Shear	46
4.2	Method 1-Elastic calibration	49
4.2.1	Connector stiffness calibration	49
4.2.2	Results	51
4.2.3	Calibration through average stiffness values	56
4.3	Method 2-Plastic calibration	58
4.3.1	Results	60
4.4	Method 3-Calibration using a experimental single lap shear	65
4.4.1	Results	67
4.5	Bearing/pull-trough ratio (n)	72
4.5.1	Concluding Remarks	74
5	Conclusion and Future Work	77
5.1	Conclusion	77
5.2	Future Work	78
	Bibliography	79

List of Figures

3.1	Visual inspection of cure-induced warpage in the manufactured composite plates: top and side profile,(Join3D).	12
3.2	Manufacturing process of the composite laminates: (a) plate preparation , and (b) autoclave cure cycle, temperature (°C) and pressure (bar) over time (Join3D).	13
3.3	Geometric sketch of the bearing test specimen.	14
3.4	Experimental bearing test overview: (a) composite specimen showing the bearing failure mode around the fastener hole; (b) INSTRON 5900R universal testing machine, hydraulic grips and an adapted bearing testing rig with specimen.	15
3.5	Load-displacement curves under bearing loading conditions for the different configurations.	16
3.6	Experimental pull-through test overview: (a) square composite specimen showing the typical pull-through failure mode around the central hole; (b) INSTRON 5900R universal testing machine configuration and the dedicated pull-through testing fixture (Join3D).	17
3.7	Load-displacement curves under pull-through loading conditions for the different configurations.	18
3.8	Used rig for BEA/PT testing as suggested by (Furtado et al., 2023).	19
3.9	Rig and composite specimen (Furtado et al., 2023).	20
3.10	3D failure interaction envelopes.	21
3.11	Experimental test setup for the single lap shear configuration, showing the composite joint gripped by hydraulic jaws of the INSTRON universal testing machine.	22
3.12	Single-Lap shear norm sketch (ASTM International, 2018b)	23
3.13	Geometric configurations of single-lap shear joint specimens.	24
3.14	Overview of the different configurations tested.	24
3.15	Force-Displacement (1 Bolt QQI).	25
3.16	Failure analysis for specimen 1Bolt_1.	25
3.17	Failure analysis for specimen 1Bolt_2.	26
3.18	Failure analysis for specimen 1Bolt_3.	26
3.19	Force-displacement for QI layups	27
3.20	Force-displacement for H layups	28
3.21	Force-Displacement for H90 layups	28
3.22	Force-displacement curves (1Bolt, 2Bolt, and 3Bolt) for the QQI and DDQI laminate configurations.	30
3.23	Force-displacement curves (1Bolt, 2Bolt, and 3Bolt) for the QH and DDH laminate configurations.	31

3.24	2x1QH, progressing bearing followed by delamination.	31
3.25	Force-displacement curves (<i>1Bolt</i> , <i>2Bolt</i> , and <i>3Bolt</i>) for the QH90 and DDH90 laminate configurations.	32
3.26	3x1QH90, net-tension failure	32
3.27	Side view of the single-lap shear joint test 1x1 QQI, showing the comparison between the (a) initial and (b) final stages of the DIC capture, highlighting the secondary bending and joint rotation.	33
3.28	Force-Displacement DIC 1x1 laminates.	34
3.29	Force-Displacement DIC 2x1 laminates.	35
3.30	Force-Displacement DIC 3x1 laminates.	36
3.31	3D failure interaction envelopes mapping Transversal Bearing (f_{TB}), Longitudinal Bearing (f_{LB}), and Pull-through (f_{PT}) forces for different configurations.	38
3.32	3D failure interaction envelopes mapping Transversal Bearing (f_{TB}), Longitudinal Bearing (f_{LB}), and Pull-through (f_{PT}) forces for QH and DDH configurations.	38
4.1	Three-dimensional finite element mesh configurations, boundary conditions, and localized refinement details for both single-bolt bearing and single-lap shear numerical models.	44
4.2	Three-dimensional finite element mesh configurations, boundary conditions, and geometric definitions of the single-bolt testing specimen components.	45
4.3	Numerical finite element configurations for the 1×1 single-bolt simulation setup.	46
4.4	Model 2x1	46
4.5	Model 3x1	47
4.6	Boundary conditions	47
4.7	Numerical modeling details of the joint kinematics:(a) side view of the single-lap assembly showing the Cartesian connector wire bridging adjacent reference points ($RP-1$ and $RP-2$), and (b) top view of the radial distribution lines loading the hole boundary mesh.	48
4.8	Stiffness calibration for both tests.	50
4.9	Intersection between the combination of CTF1, CTF2 and CTF3 with the QQI failure envelope.	51
4.10	Linear elastic curves extracted for the 1×1 configurations compared with experimental curves	53
4.11	Linear elastic curves extracted for the 2×1 configurations compared with experimental curves, note that some plots are not using DIC load-displacement curve	54
4.12	Linear elastic curves extracted for the 3×1 configurations compared with experimental curves, note that some plots are not using DIC load-displacement curve.	55
4.13	Modified linear elastic response curves extracted for the 1×1 configurations.	57
4.14	Comparison of the load-displacement curves for the bearing behavior between the experimental data (Experimental Bearing) and the calibrated numerical model (Abaqus).	58
4.15	Multi-linear connector plasticity curves extracted for the 1×1 configurations, note that some plots are not using DIC load-displacement curve.. . . .	61

4.16	Multi-linear connector plasticity curves extracted for the 2×1 configurations, note that some plots are not using DIC load-displacement curve..	62
4.17	Multi-linear connector plasticity curves extracted for the 3×1 configurations, note that some plots are not using DIC load-displacement curve.	63
4.18	1x1QH90 CTF1 and CTF3 Load-Displacement curve.	64
4.19	Elastic calibration through 1x1 QQI.	65
4.20	Calibration through QQI Load-Displacement curve.	66
4.21	Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the 1×1 single-bolt joint configurations across different laminate configurations with the exception of 2x1 DDH an 3x1 QQI.	69
4.22	Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the multi-bolt (2×1 array) joint configurations across different laminate configurations with the exception of 2x1 DDH an 3x1 QQI.	70
4.23	Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the multi-bolt (3×1 array) joint configurations across different laminate configurations with the exception of 2x1 DDH an 3x1 QQI.	71
4.24	Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 1.	72
4.25	Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 2.	73
4.26	Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 3.	73
4.27	Normal method analysis.	74
4.28	Plastic method analysis.	74
4.29	SLS method analysis.	74
4.30	Comparison of n factor vs displacement for all three methods.	74

List of Tables

3.1	Laminate configurations, ply counts, and nominal thickness measurements. .	12
3.2	Experimental bearing average load values in kN for the tested laminates. . .	16
3.3	Average Pull-Through values in kN for different laminates.	18
3.4	Average load values in kN used to generate the failure envelope.	22
3.5	Summary of experimental values for different laminate configurations in kN.	37
4.1	Elastic properties and mass density of the composite material.	42
4.2	Connector stiffness values for BEA and Pull-Through.	49
4.3	First non-linearity point for 1x1 configuration.	51
4.4	First non-linearity point for 2x1 configuration.	52
4.5	First non-linearity point for 3x1 configuration.	52
4.6	Average stiffness values for Pull-Through and Bearing.	56
4.7	First non-linearity on Abaqus curve compared to the same displacement on the experimental curve.	56
4.8	Plasticity values taken from bearing curves for each layup sequence.	59
4.9	First non-linearity points for different configurations: Numerical vs. Exper- imental.	64
4.10	Connector stiffness property.	65
4.11	Plasticity calibration data for the numerical model.	66
4.12	First non-linearity values for each configuration: Numerical vs. Experimen- tal and relative error.	67
4.13	Exact value of n at the failure envelope intersection.	73
4.14	Comparison of the intersection values of n for QOI laminate across different bolt configurations.	74
4.15	Comprehensive synthesis of the advantages, limitations, results, and n -ratio evolution of the three calibration methods.	75

Document notation

Abbreviations and Acronyms

ASTM	American Society for Testing and Materials
BC	Boundary Conditions
BEA	Bearing Strength Test
BEA30	Bearing Test loaded at an off-axis angle of 30 degrees
BEA60	Bearing Test loaded at an off-axis angle of 60 degrees
BEA90	Bearing Test loaded at a transverse angle of 90 degrees
CFRP	Carbon-Fibre-Reinforced Polymer
CLT	Classical Laminate Theory
CONN3D2	Abaqus 3D Two-Node Discrete Connector Element
DD	Double-Double (Laminate architecture)
DDH	Double-Double Hard laminate layout
DDH90	Double-Double Hard laminate layout rotated 90 degrees
DDQI	Double-Double Quasi-Isotropic laminate layout
DIC	Digital Image Correlation
FHC	Filled Hole Compression
FHT	Filled Hole Tension
LQL	Legacy QUAD Laminate
LVDT	Linear Variable Differential Transformer
OHC	Open Hole Compression
OHT	Open Hole Tension
PT	Fastener Pull-Through Resistance test
QH	Standard Quad Hard laminate layout
QH90	Standard Quad Hard laminate layout rotated 90 degrees
QI	Quasi-Isotropic laminate
QQI	Standard Quad Quasi-Isotropic laminate layout
RP	Reference Point
SLS	Single-Lap Shear
3D	Three-Dimensional

Roman letters

$[A^*]$	Normalized in-plane laminate stiffness matrix
d	Nominal bolt/fastener diameter (e.g., M6)
e	Bolt hole edge distance to specimen boundary
E_x	Longitudinal Young's modulus of the laminate
E_1	Longitudinal Young's modulus of a single ply
E_2	Transverse Young's modulus of a single ply
F	Applied external joint force
F_{max}	Ultimate structural failure load / Peak force
f_{LB}	Normalised Longitudinal Bearing failure force component
f_{PT}	Normalised Pull-Through failure force component
f_{TB}	Normalised Transversal Bearing failure force component
G_{12}	In-plane ply shear modulus
K_e	Linear elastic structural joint stiffness
K_{conn}	Stiffness assigned to individual connector element components
p	Bolt pitch spacing distance
t	Total multi-ply laminate thickness
u	Total external or structural displacement
w	Width of the composite laminate specimen

Greek letters

δ	Localised internal displacement / Connector elongation component
δ_p	Localised plastic or permanent residual displacement boundary
η	Combined load index ratios
ν_{12}	Major in-plane ply Poisson's ratio
σ_b	Bearing stress at the bolt hole interface
σ_{bu}	Ultimate bearing strength of the structural laminate
ϕ	First defining angle pair of a Double-Double ply block $[\pm\phi/\pm\psi]$
ψ	Second defining angle pair of a Double-Double ply block $[\pm\phi/\pm\psi]$

Chapter 1

Introduction

1.1 Motivation

Composite laminates are increasingly favored in aerospace engineering because their exceptional strength-to-weight ratio yields much lighter aircraft structures compared to traditional metals. Within these assemblies, mechanical fastening via bolted joints remains the standard joining method, valued for its outstanding robustness and straightforward disassembly during maintenance. This method traditionally involves stacking plies at standard orientations ($0^\circ, 90^\circ, \pm 45^\circ$), following strict industry guidelines that demand symmetry and balance in the laminate design. Although such rules are intended to ensure reliable performance under various loading conditions, they often lead to overly conservative structures that are limited more by prescriptive design practices than by performance requirements. In response to these limitations, a novel approach called Double-Double (DD) laminates has been proposed. Unlike the traditional quad method, DD laminates use two ply orientations per building block arranged in a bi-angle configuration, defined as $[\pm\phi / \pm\psi]_n$, where ϕ and ψ can represent any angle from 0° to 90° and n represents the number of repetitions.

Conventional modeling approaches rely heavily on detailed numerical frameworks to predict joint performance and failure mechanisms. While these highly detailed models offer great accuracy, they are computationally expensive and often impractical for large-scale industrial analysis. This challenge is further compounded when dealing with multi-bolt joint configurations under complex operational loads. To address this, there is an urgent industrial need for simplified macroscale engineering models such as connector based formulations that retain predictive capability while remaining computationally affordable for industrial-scale multi-bolt applications.

The first objective of this thesis is to experimentally characterize the mechanical response of Double-Double (DD) laminates and directly compare their structural behavior with conventional quad-ply bolted joints. Understanding the performance of this new stacking sequence requires a comprehensive testing campaign pure bearing, pull-through, combined bearing/pull-through, and multi-bolt Single-Lap Shear arrays. By leveraging

advanced diagnostic tools such as Digital Image Correlation (DIC) to capture precise full-field displacement fields and load-sharing trends, this research maps the physical behavior under distinct load cases to construct experimental failure envelopes for different laminate configurations.

The second major goal of this thesis is to define and validate a numerical methodology to predict joint behavior compatible with industrial workflows. Implementing the experimentally derived failure envelopes into Abaqus, this work develops low-fidelity connector-based formulations tailored for industrial-scale multi-row joint configurations. Three distinct connector calibration methodologies are explored combining elastic stiffness, plasticity, and calibration through experimental single lap shear tests to accurately capture structural non-linearity and load distribution trends.

1.2 Document layout

This thesis is structured into five chapters. Chapter 2 provides a comprehensive literature review, covering the fundamental mechanics of advanced composite materials, the comparison between traditional quad-laminates and Double-Double architectures, standard joint testing configurations, and the evolution of numerical modeling strategies for composite bolted connections. Chapter 3 outlines the experimental tests, detailing the material selection, manufacturing procedures, and testing setup. It presents the empirical results for pure bearing, pull-through, and combined bearing/pull-through configurations, alongside the results for multi-bolt Single-Lap Shear joint arrays (1×1 , 2×1 , and 3×1) integrated with Digital Image Correlation (DIC) monitoring. Chapter 4 details the computational simulation framework, focusing on the development and implementation of low-fidelity models using Abaqus with the use of connector elements. It describes the three proposed calibration methodologies and evaluates their predictive accuracy against the experimental load-displacement responses and multi-bolt failure behavior. Chapter 5 summarizes the main findings and structural contributions of the thesis and suggests directions for future research and potential improvements.

Chapter 2

State-of-art and literature review

2.1 Composite materials

Composite materials are indispensable in modern engineering, mostly in sectors where high performance, low weight, and structural reliability are critical. These materials are the combination of two or more distinct materials typically fibers (reinforcement) embedded in another material (matrix) that holds them together in a desired configuration.

One of the main reasons for the success of such configurations is that a material in fiber form presents much superior mechanical properties than the same material in bulk form (de Moura et al., 2005). Additionally, the synergetic effect brought by the combination of the phases creates a material with functional properties that surpass those of the individual constituents. Fiber Reinforced Polymers (FRPs) offer a high strength-to-weight ratio, corrosion resistance, and design versatility, positioning them at the forefront of technological innovation. In the aeronautical, composites have been used since the 1970s, military applications were initially more prominent, civil aviation usage has increased dramatically since the beginning of the 21st century. Modern aircraft, incorporate more than 50% in weight composite materials in their primary structures, including fuselage sections and wings (Gibson, 2016).

2.2 QUAD and Double-Double laminates

The use of traditional composite laminates, characterized by a discrete collection of 0° , $\pm 45^\circ$, and 90° plies commonly referred as Legacy QUAD Laminates (LQL) goes back to the emergence of boron and carbon fiber reinforced polymers in the 1960s (Tsai, 2021). For over six decades, four orientations have served as the industry standard, primarily because they allow for a simplified “rule of mixtures” approach to stiffness and were manageable for manual calculations during the early stages of aerospace composite design (Shrivastava et al., 2020).

However, LQL configurations are limited by restrictive rules of design that significantly influence the design space. These include the requirement for mid-plane symmetry to

prevent out-of-plane warping during curing and the 10% rule, which mandates that at least 10% of each of the four orientations must be present to ensure a minimum level of robustness (Tsai, 2021). Such constraints often result in designs that are structurally inefficient and overweight. Because the LQL relies on a discrete set of angles, the number of possible stacking permutations grows exponentially with thickness.

To overcome the inherent limitations of LQL, a type of laminates known as Double-Double (DD) was proposed by Tsai and Melo (Tsai and Melo, 2015) and further developed into a comprehensive design framework (Tsai, 2021). Unlike the rigid $0/\pm 45/90$ scheme, DD configurations are defined by two pairs of angle-ply, typically denoted as $[\pm\phi/\pm\psi]$.

The fundamental breakthrough of the DD is laminate homogenization. By using thin ply assemblies, DD laminates behave as a homogeneous material where the stacking sequence effect is effectively decoupled from the in-plane stiffness (Vermes et al., 2020). Which allows for the adoption of a universal, self-repeating stacking sequence, such as $[\phi/-\psi/-\phi/\psi]_{rT}$, which eliminates the need for mid-plane symmetry and simplifies the characterization of material properties through the use of Tsai’s Modulus (Tsai, 2021).

2.2.1 Equivalent Stiffness Search

Every existing QUAD laminate can be mathematically mapped to a DD counterpart by matching the normalized in-plane stiffness matrix $[A^*]$ (Tsai, 2021). This DD-search allows engineers to retain the global stiffness requirements of a proven design while unlocking the manufacturing advantages of the DD architecture. In the field of composite structural design, particularly within the aerospace industry, laminates are categorized based on their directional stiffness and stacking sequence. The terms “Hard” and “Soft” are widely used to describe the polar behavior of a laminate’s elastic properties relative to the primary loading axis (conventionally the 0° axis).

2.2.1.1 Hard Laminates (High 0° Content)

A laminate is formally defined as “Hard” when its stacking sequence is dominated by 0° plies, typically exceeding 50% to 60% of the total layup. According to Classical Laminate Theory (CLT), increasing the percentage of fibers aligned with the load path maximizes the longitudinal Young’s modulus (E_x). Historically, as documented in the MIL-HDBK-17-3F, this configuration is selected for primary structural members like wing spar caps or fuselage stringers, where maximum axial stiffness and compression strength are required (U.S. Department of Defense, 2002). However, “Hard” laminates exhibit a more brittle failure mode (Hart-Smith, 1987). Their lack of off-axis fibers ($\pm 45^\circ$) results in high stress concentration factors, making them highly sensitive to notches, bolt holes, and impact damage.

2.2.1.2 Soft Laminates (High $\pm 45^\circ$ or 90° Content)

A Soft laminate is characterized by a lower percentage of 0° plies and a predominance of $\pm 45^\circ$ or 90° plies. As explained in practical design manuals, the softness refers to the laminate's reduced axial stiffness and increased compliance under longitudinal loads (Niu, 1992). A critical engineering principle is that a Hard laminate becomes functionally Soft if the entire layup is rotated 90° relative to the applied load. In this state, the 0° fibers are now oriented transversely, leaving the matrix and off-axis fibers to carry the load. Soft laminates are strategically utilized in damage-tolerant designs. Because they are more compliant, they allow for localized strain redistribution around fasteners and cutouts, effectively blunting crack propagation and increasing the residual strength of the component.

2.2.2 Manufacturing and Structural Efficiency

One of the primary advantages of DD laminates is the significant reduction in manufacturing complexity. QUAD laminates require multiple ply orientations, which increases lay-up time, tool changes, and the likelihood of misalignment during fabrication. On the other hand, DD laminates minimize the number of ply angles, making them highly compatible with Automated Fibre Placement (AFP) processes. This reduction in angle transitions improves deposition speed, reduces steering defects, and lowers overall production costs (Lopes et al., 2009; Gürdal and Olmedo, 1993).

Furthermore, DD laminates allow designers to tailor stiffness more precisely along dominant load paths by overcoming the inherent mathematical limitations of traditional layouts. While conventional QUAD configurations are restricted by discrete ply-percentage increments due to their fixed four-angle ($0^\circ, 90^\circ, \pm 45^\circ$) architecture, DD laminates eliminate these strict discretization constraints. Instead of relying on rigid percentage mixtures that often force an overly conservative design, the bi-angle $[\pm\phi/\pm\psi]_n$ formulation allows for continuous adjustments of the ply angles. This capability enables an exact optimization of the anisotropic stiffness and strength fields to perfectly match complex multi-axial load paths, maximizing structural efficiency and eliminating unnecessary weight penalties (Ibrahim et al., 2014; Barbero, 2018).

2.2.3 Optimization and Performance

DD laminates also offer advantages in optimization workflows, their reduced number of ply orientations simplifies the design space and enables faster convergence in optimization algorithms. Recent studies show that DD laminates can achieve stiffness equivalence to Quad laminates while requiring fewer design variables, making them well suited for structural optimization in early design phases (Gomes et al., 2023; Abdalla et al., 2007).

In specific loading scenarios, as axial or shear-dominated loads, DD laminates can outperform QUAD configurations by providing higher directional stiffness with fewer plies.

The reduced number of ply interfaces can lead to lower interlaminar stress concentrations, while DD laminates can be more sensitive to transverse loading, their simplified architecture reduces the number of potential delamination planes, which is advantageous where through-thickness stresses are minimal (Soutis, 2005).

2.2.4 Experimental Characterization and Mechanical Behavior

The transition from the discrete four-angle standard to the homogenized bi-angle layout significantly reshapes the laminate’s internal stress fields, failure paths, and directional response under fundamental mechanical loads (Tsai, 2022).

Experimental data under unnotched compression demonstrates that DD laminates emulated to match LQL stiffness targets consistently achieve higher absolute compressive strength and superior resistance to micro-buckling (Hawkins et al., 2026). In traditional QUAD layups, thick ply blocks (often up to 20 plies per repeating unit) promote substantial out-of-plane shear stress concentrations at the interfaces, making the structure highly susceptible to localized fiber micro-buckling. In DD stacking the transverse stresses are spread uniformly across the thickness. Localized stress suppression increases the critical buckling threshold of the continuous load-bearing fibers. Under pure unnotched tension, the mechanical efficiency remains heavily dependent on the volume fraction of fibers aligned exactly parallel to the loading axis (0°) (Hawkins et al., 2026). While LQL layups possess a rigid percentage allocation that matches the axial load path discretely, DD laminates achieve equivalent stiffness through optimized off-axis combinations (Φ, Ψ). Consequently, DD laminates may exhibit a comparable or slightly lower ultimate unnotched tensile strength, though they yield a much smoother elastic deformation profile without internal matrix cracking spikes.

When geometric discontinuities are introduced, the anisotropic stress concentration factors and local material compliance govern the damage progression. In Open-Hole Compression (OHC) testing, the uniform distribution of thin angle pairs in the DD framework effectively mitigates the local delamination peeling stresses that normally occur around an open hole boundary in traditional quadriaxial laminates, thereby preventing premature buckling propagation from the hole edges and resulting in a higher residual compressive capacity. Similarly, under Open-Hole Tension (OHT) scenarios, DD configurations demonstrate exceptional stress-redistribution capabilities. The continuous variations allowed by the $[\pm\phi/\pm\psi]$ pairs minimize the stark stiffness mismatches between adjacent plies that typically induce high interlaminar shear strains in traditional laminates, effectively blunting the stress peaks at the hole boundary and delaying net-tension fiber fracture across the ligament area (Hawkins et al., 2026).

The mechanical integrity of tapered panels with variable thickness provides another clear structural distinction between the two configurations. For DD laminates, thickness modification is achieved through a purely global geometric approach by changing the repetition integer n of the core stack (Tsai et al., 2024). This ensures that all ply drops are

strictly restricted to the exterior surfaces of the laminate, keeping the internal continuity of the load paths entirely uninterrupted. From a mechanical standpoint, this eliminates the internal resin-rich pockets, local eccentricities, and interlaminar shear stress spikes typically found in LQL internal ply drops, thereby preserving the full theoretical strength allowables of the composite stack (Tsai et al., 2024).

2.3 Mechanically-fastened joints and Notch Sensitivity Characterization

To ensure the structural integrity, airworthiness, and fail-safe performance of primary and secondary aerospace components, we must rigorously test, characterize, and monitor mechanical joints, whether bolted, riveted, bonded, or hybrid (Camanho and Matthews, 1997). Bolted configurations, for instance, are the standard choice for joining large-scale composite assemblies like fuselage sections or wing skins to spars (Hart-Smith, 1985). However, drilling a fastener hole inevitably cuts through the reinforcing fibers, disrupting the continuous load path and creating severe local stress concentrations. Because composites are highly sensitive to these localized stress fields, they often trigger complex, progressive damage modes that interact with each other. This typically starts with bearing failure via local matrix crushing and fiber micro-buckling, but can quickly escalate to net tension failure across the ligament, shear-out along the loading planes, or catastrophic delamination driven by high interlaminar shear stresses (Camanho and Matthews, 1997).

Isolating how specific geometric and manufacturing choices affect joint strength requires standardized testing under quasi-static, fatigue, and dynamic loads (Camanho and Matthews, 1997; ASTM International, 2018b). Key parameters like the stacking sequence, bolt preload, clearance tolerances, and the thickness-to-diameter ratio (t/D) all play a role. Mapping these multi-parametric effects is essential for modern structural optimization in aeronautics. To capture this behavior accurately, current experimental research moves beyond global metrics, relying heavily on in situ non-destructive evaluation (NDE). Techniques like Digital Image Correlation (DIC) allow us to map full-field surface strains right around the washer boundary, while Acoustic Emission (AE) sensors help detect micro-cracking and early fiber breakage long before the joint actually fails.

To standardize the evaluation of these interaction mechanisms, several distinct test configurations have been developed and widely adopted in aerospace engineering.

2.3.1 BEA – Bearing Strength Test

The Bearing (BEA) test evaluates the bearing response of a composite laminate subjected to compressive loading through a fastener. This test is essential for determining bearing strength, stiffness, and the onset of damage such as matrix cracking, fiber kinking, and local delamination. Bearing failure is often desirable in design because it is progressive and non-catastrophic, providing warning before ultimate failure (ASTM International, 2018b;

[Matthews and Kilty, 1983](#)).

2.3.2 FHT – Filled Hole Tension

The Filled Hole Tension (FHT) test assesses the tensile strength of a laminate containing a fastener inserted into the hole. This configuration simulates real joint conditions where the presence of the fastener constrains deformation and alters stress distribution. FHT tests help identify failure modes such as net tension fracture, shear-out, and mixed-mode failures ([Camanho and Matthews, 1997](#)).

2.3.3 OHT – Open Hole Tension

The Open Hole Tension (OHT) test measures the tensile strength of a laminate with an open, unloaded hole. This test isolates the effect of the hole itself, providing critical data for damage tolerance, stress concentration effects, and laminate design optimization. OHT results are widely used in certification allowables and numerical model validation ([ASTM International, 2018a](#); [Kim and Daniel, 1997](#)).

2.3.4 OHC – Open Hole Compression

The Open Hole Compression (OHC) test evaluates the compressive strength of a laminate containing an open hole. This configuration is particularly relevant for assessing stability related failure modes such as micro buckling, fiber kinking, and delamination propagation under compressive loads. OHC performance is crucial for components subjected to compressive stresses, such as fuselage skins and wing panels ([ASTM International, 2020](#); [Soutis, 2005](#)).

2.3.5 FHC – Filled Hole Compression

The Filled Hole Compression (FHC) test examines the compressive behavior of a laminate with a fastener inserted in the hole. The presence of the fastener modifies the stress field and can delay or alter failure mechanisms compared to OHC. FHC tests are essential for evaluating bearing by compression behavior and for validating joint design in compressive load paths ([Camanho and Matthews, 1997](#)).

2.4 Low Fidelity models

2.4.1 Introduction and Computational Efficiency Imperatives

In the preliminary design, structural sizing, and optimization phases of large-scale aerospace components, minimizing computational cost is a primary engineering objective. An aircraft assembly can contain thousands of fasteners, making detailed volumetric discretization of every single joint computationally prohibitive. Consequently, low-fidelity models

have become indispensable tools for engineering practice, sacrificing highly localized stress gradients in favor of rapid execution, low degrees of freedom, and scalability. The primary challenge in low-fidelity modeling lies in preserving macroscale behavior, such as structural compliance, load distribution among multi-bolt configurations, and initial failure thresholds, without solving for extensive numerical systems (McCarthy et al., 2005b; Thoppul et al., 2009). These simplified frameworks must accurately substitute complex physical interactions, such as contact friction, bolt-hole clearance, and laminate degradation, through lumped parameters, simplified kinematics, or closed-form analytical equations to ensure reliable global structural evaluations under realistic operational conditions (Camanho and Lambert, 2006).

2.4.2 Evolution of Simplified Connector and Cartesian Frameworks

One of the most efficient low-fidelity methodologies for estimating load distribution in multi-fastener joints relies on structural connector elements, where the physical fastener is replaced by a simplified mathematical link (Kim et al., 2007). In these frameworks, the bolt, the surrounding plates, and the shear interactions are represented via discrete bushing or beam connector components assigned a specific flexibility or stiffness (Chandregowda and Suresh, 2018; Gunbring, 2008). Central to this approach is the Cartesian coordinate formulation, where the joint's complex three-dimensional kinematics are uncoupled into independent structural springs aligned with the local Cartesian axes. This allows the shear stiffness (k_x, k_y), axial stiffness (k_z), and rotational degrees of freedom to be populated directly into a localized stiffness matrix. To capture the proper elasticity of the joint without volume discretization, several semi-empirical formulations—such as those developed by Huth (Huth, 1984), Grumman, Swift, and Boeing (Eremin and Ivanov, 2021) are applied to calculate the appropriate connector flexibility based on plate thicknesses (t_1, t_2), fastener diameters (d), and elastic moduli (E_1, E_2, E_3).

2.4.3 Macroscale Failure Envelope and Simplified Fastener Prediction

Predicting ultimate failure within an efficient structural design framework requires shifting from explicit 3D solid continuum damage models toward macroscale semi-empirical failure criteria implemented into simplified joint representations (Barbero, 2018). While legacy methodologies developed for traditional Quad laminates typically rely on characteristic material distances or point-stress approximations to assess first-ply and progressive failure under combined bearing and bypass loads (Camanho and Lambert, 2006), the structural behavior of novel Double-Double (DD) configurations demands an analytical formulation free from conventional ply-angle restrictions Tsai (2022).

Macroscopic force fields extracted from simplified multi-bolt models are evaluated using a custom three-dimensional ellipsoidal failure envelope (Volpi, 2024; Santos, 2025). The global finite element multi-bolt configurations leverage Abaqus-defined Cartesian connector elements linked to circular regions of structural influence to replicate the physical clamping

and stiffness effects of the washer assemblies [Dassault Systèmes \(2024\)](#); [Santos \(2025\)](#). The connector stiffness components are calibrated a priori through an automated compliance matching routine to reproduce the pure bearing and pure pull-through response curves established via high-fidelity virtual testing protocols [Furtado et al. \(2023\)](#); [Santos \(2025\)](#).

The structural integrity and ultimate loading capacity of the assembly are quantified by tracking the multi-axial connector forces, the longitudinal bearing component (f_{PB_0}), the transverse bearing component ($f_{PB_{90}}$), and the out-of-plane pull-through force (f_{PT})—until they intersect the multi-axial failure boundary ([Volpi, 2024](#)). Through mathematical optimization and ellipsoidal fitting of the failure loci gathered from high-fidelity building-block virtual tests, the macroscale quadratic failure envelope for the tailored DD layup is formulated as:

$$\frac{f_{PB_0}^2}{A^2} + \frac{f_{PB_{90}}^2}{B^2} + \frac{f_{PT}^2}{C^2} = 1 \quad (2.1)$$

where the denominators A , B , and C represent the standardized ultimate strength scaling parameters under pure uncoupled loading states for the given material configuration [Tsai and Wu \(1971\)](#); [Volpi \(2024\)](#).

Compared to standard legacy configurations, this failure surface extends traditional unit circle concepts into three-dimensional joint load spaces [Vermees et al. \(2020\)](#). Ultimate failure within this low-fidelity structural shell environment is defined deterministically at the exact locus where the multi-axial connector forces violate this macroscopic boundary surface [Santos \(2025\)](#); [Volpi \(2024\)](#). This methodology substantially lowers computational cost, reducing overall analysis runtimes to a fraction of the time required by progressive continuum damage frameworks while preserving robust predictive capabilities across complex multi-fastener composite joint layouts [Santos \(2025\)](#).

Chapter 3

Experimental Test

This chapter presents the comprehensive experimental campaign conducted to characterize the mechanical response and failure behavior of the Quad and Double-Double (DD) composite laminates. The experimental part is structured into four main testing phases. First, standardized pure bearing tests and pure pull-through tests are performed to establish baseline properties under in-plane and out-of-plane loading regimes, the results for pull-through were taken from the project [Join3D](#). Second, a multi-axial experimental rig is employed to investigate the combined mixed-mode bearing/pull-through behavior at distinct load ratios (η) again taken from project [Join3D](#). with the exception of the (η) 0.8 that new tests were made. Third, multi-bolt scalability and damage progression are evaluated through single-lap shear bolted joint configurations, monitored via full-field Digital Image Correlation (DIC) analysis. Finally, the gathered experimental data is synthesized to generate the analytical 3D failure envelopes for each laminate architecture.

3.1 Material Selection

A unidirectional (UD) carbon fiber prepreg from Angeloni (UD HS 150 IMP509HM), with a nominal thickness of 0.15 mm, was chosen to produce the layup sequences. The laminate design process focused on comparing legacy Quad configurations against equivalent Double-Double (DD) configurations under both Quasi-Isotropic (QI) and Hard (H) structural profiles. These DD and Quad configurations were designed to be equivalent in stiffness, following the methodology detailed in [Santos \(2025\)](#) to obtain a DD layup that matches the stiffness properties of a standard Quad configuration.

The specific configurations selected for this tests are detailed in Table [3.1](#), which includes the standard quasi-isotropic (QI) and hard (H) layups. Additionally, two soft (S or 90°) configurations were derived by rotating the QH and DDH specimens by 90° to evaluate their orthogonal, lower-stiffness structural responses.

Table 3.1: Laminate configurations, ply counts, and nominal thickness measurements.

Type	Layup	No. of Plies	Thickness
QQI	$[45/0/-45/90]_{3S}$	24	3.6 mm
DDQI	$[22.5/-67.5/-22.5/67.5]_{6T}$	24	3.6 mm
QH	$[45/90/-45/0/0/45/0/0/-45/0]_S$	20	3.0 mm
DDH	$[57/-12/0/12/-57]_{2S}$	20	3.0 mm

The reason the DDH layup is not a pure DD configuration and exhibits symmetry stems from the fact that the laminate experienced warpage after curing, as illustrated in Figure 3.1. To mitigate this issue, the stacking sequence was modified to a symmetric layup.

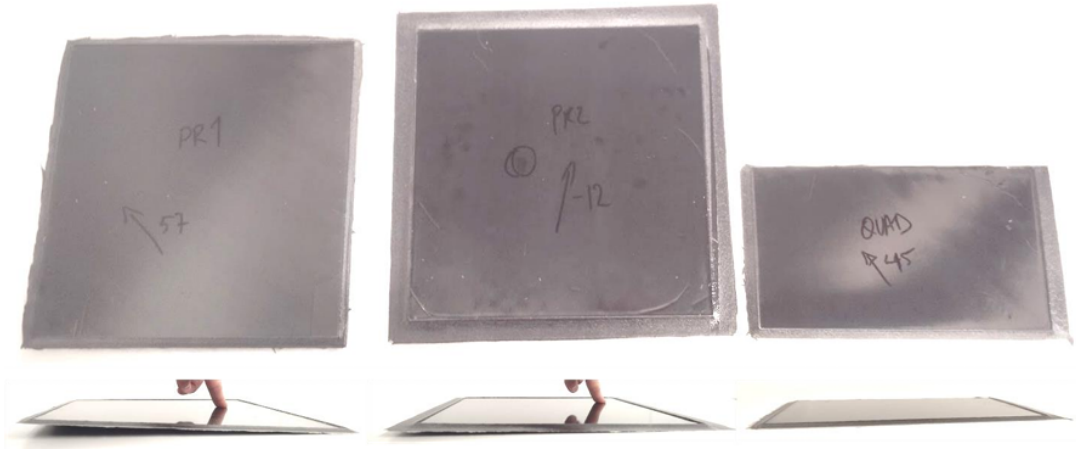
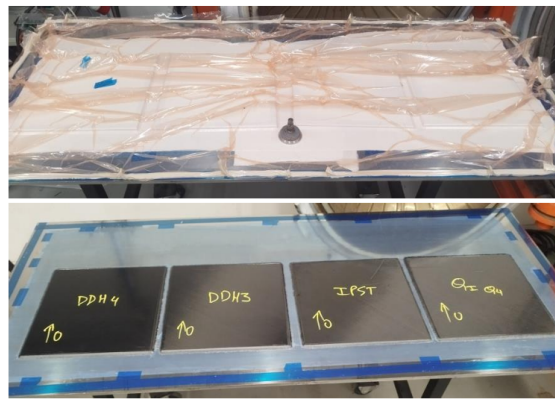


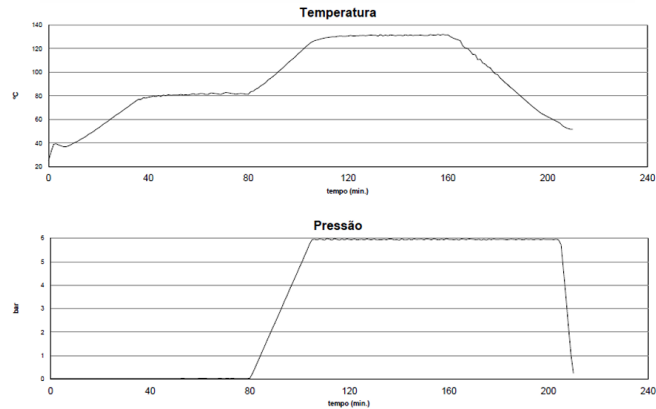
Figure 3.1: Visual inspection of cure-induced warpage in the manufactured composite plates: top and side profile,([Join3D](#)).

3.1.1 Laminate Manufacturing

The carbon fiber-reinforced polymer (CFRP) plates were processed within a controlled clean room environment. To prevent internal voids and entrapment of air, a debulking stage was performed every eight to ten layers during the stacking sequence. This process was executed under a vacuum bag at a pressure not exceeding 0.1 bar for a minimum duration of twenty minutes, utilizing a perforated Teflon film at the outer plies of the sub-laminates to facilitate air evacuation. Following the complete stacking process, the laminates were consolidated in an autoclave following the supplier-recommended cure cycle as illustrated in Figure 3.2.



(a) Vacuum bagging assembly and cured composite plates.



(b) Autoclave temperature and pressure profiles.

Figure 3.2: Manufacturing process of the composite laminates: (a) plate preparation , and (b) autoclave cure cycle, temperature ($^{\circ}\text{C}$) and pressure (bar) over time ([Join3D](#)).

Machined aluminum caul plates were placed on both surfaces of the composite stack to ensure optimal pressure distribution and guarantee post-cure surface flatness. Finally, the consolidated plates were sectioned using a diamond-blade saw cut to extract the standardized test specimens and drilled to obtain the holes.

3.2 Pure Bearing Test

For the pure bearing scenario, experimental testing was conducted following the ASTM D5961 standard to evaluate the joint response under in-plane concentrated loads ([ASTM International, 2018b](#)). To achieve this, composite specimens were machined with a nominal width of 36 mm and length of 135 mm. For each distinct laminate stacking sequence, a minimum of three tests were performed to ensure statistical reliability and to capture the characteristic scatter associated with composite failure mechanisms. The experimental procedure begins with the preparation of the coupon, which features a precision-drilled clearance hole positioned 18 mm away from the specimen end centrally, Figure 3.3. An M6 bolt (12.9 hardness) is then inserted through the hole and precisely torqued to 2.2 Nm. The choice of an M6 fastener directly aligns with typical aerospace fastening scales and allows for a realistic evaluation of the joint's stress distribution.

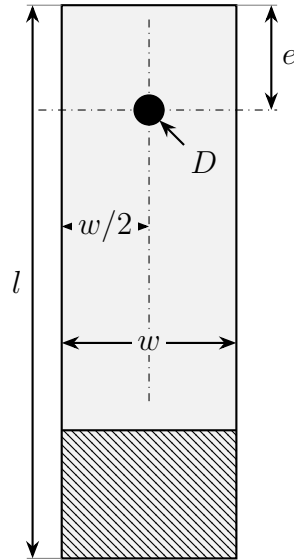


Figure 3.3: Geometric sketch of the bearing test specimen.

The entire assembly is mounted into a universal testing machine using a double-shear test fixture as shown on Figure 3.4b. This fixture ensures a symmetric load path and prevents out-of-plane bending during the loading cycle. As the displacement initiates, the machine applies a progressive tensile force that drives the rigid fastener against the internal edge of the composite hole, generating a severe, localized bearing stress field. Throughout the duration of the test, the data acquisition system continuously monitors both the applied force and the displacement. To obtain the failure envelopes, bearing tests were carried out with the specimen alignment at 0° , 30° , 60° , and 90° , for both QH and DDH, because this laminates exhibits anisotropic behavior. One example of a specimen after bearing test shown in Figure 3.4a



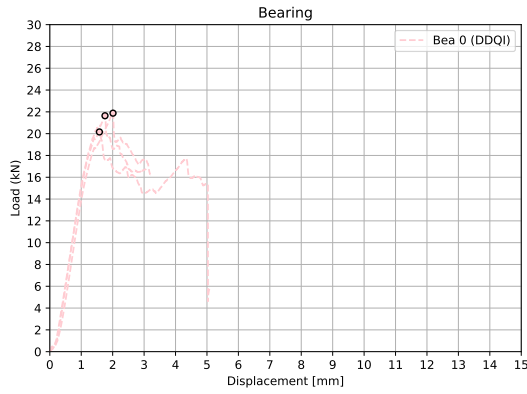
(a) QH specimen after bearing test.



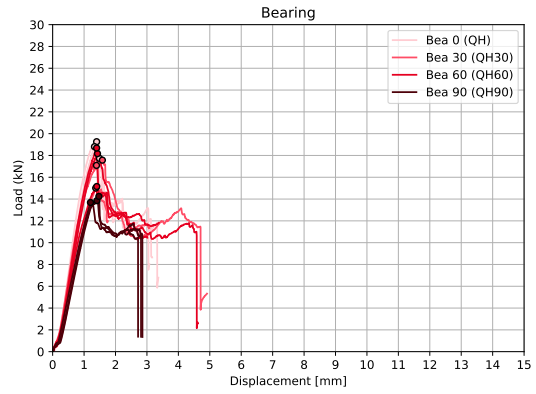
(b) Experimental test setup.

Figure 3.4: Experimental bearing test overview: (a) composite specimen showing the bearing failure mode around the fastener hole; (b) INSTRON 5900R universal testing machine, hydraulic grips and an adapted bearing testing rig with specimen.

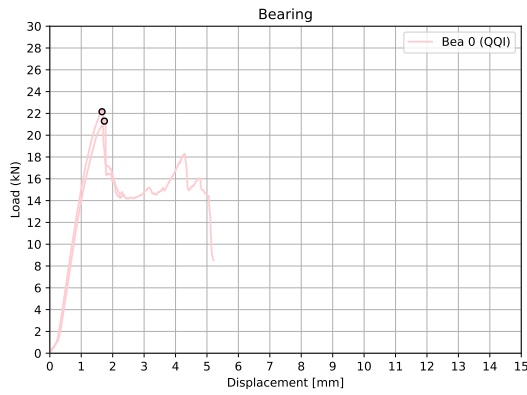
Figure 3.5 illustrates the load-displacement plots as well as the points selected to generate the failure envelope. The highest point of each curve was chosen because it signifies the beginning of strength degradation. The values presented in Table 3.2 represent the average of these points for each specific layup.



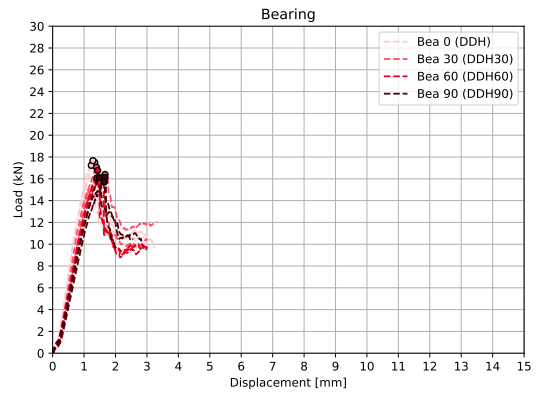
(a) DDQI configuration.



(b) QH configuration.



(c) QQI configuration.



(d) DDH configuration.

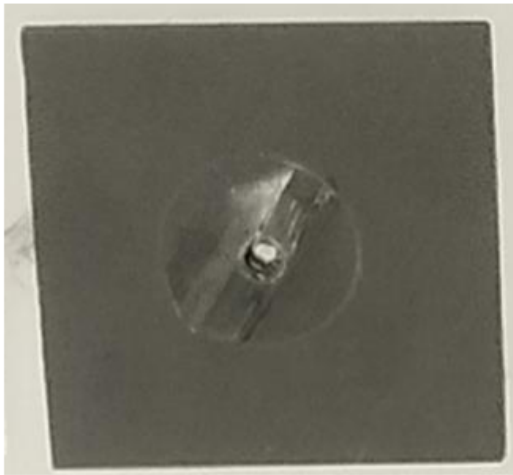
Figure 3.5: Load-displacement curves under bearing loading conditions for the different configurations.

Table 3.2: Experimental bearing average load values in kN for the tested laminates.

Property	QQI	DDQI	QH	DDH
BEA	21.72	21.23	18.81	17.47
BEA90	—	—	13.83	15.97
BEA60	—	—	17.33	16.63
BEA30	—	—	16.56	16.52

3.3 Pull-Through Test

To characterize the out-of-plane mechanical performance and the resistance of the composite architecture against multi-axial separation, pull-through testing was implemented according to the ASTM D7332 standard ([ASTM International, 2007](#)). The experimental workflow followed Procedure B, which is widely accepted in aeronautical testing as the most representative methodology for evaluating structural joints subjected to out-of-plane loads. The specimens utilized in this procedure were machined into flat square plates with precise nominal dimensions of 84 mm $\times$ 84 mm. Each plate was manufactured with a central clearance hole, precision-drilled to accommodate a standard M6 structural bolt. During the execution of the test, the square composite specimen is firmly clamped inside a rigid, specialized pull-through test fixture. This fixture features a calibrated circular support opening that surrounds the central hole, providing a symmetric boundary condition that isolates the localized failure zone while controlling the global bending deflection of the plate. Once securely clamped, the universal testing machine applies a controlled displacement of 1 mm/min, pulling the M6 fastener perpendicularly relative to the laminate plane (the through-thickness direction).



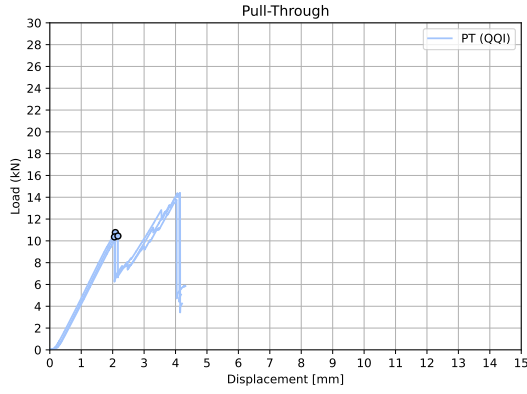
(a) Composite pull-through test specimen after failure.



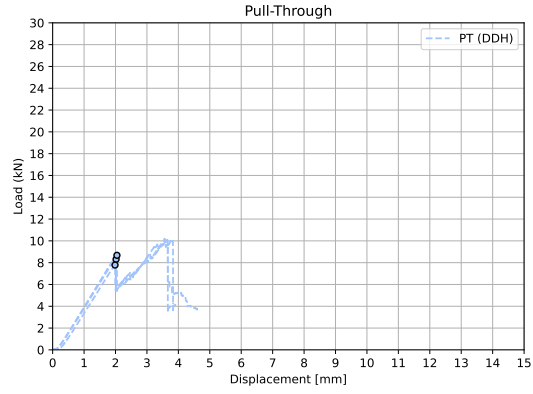
(b) Experimental test setup.

Figure 3.6: Experimental pull-through test overview: (a) square composite specimen showing the typical pull-through failure mode around the central hole; (b) INSTRON 5900R universal testing machine configuration and the dedicated pull-through testing fixture ([Join3D](#)).

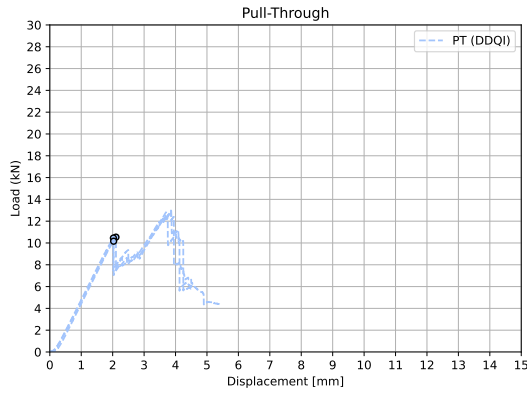
Three tests were carried out for each layups. The load-displacement plot can be seen on Figure 3.7. From the results, the peak loads and the shape of the curves for each Q-DD par are very similar to each other.



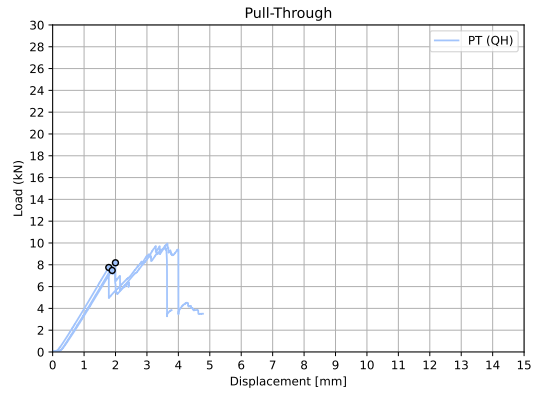
(a) QQI configuration.



(b) DDH configuration.



(c) DDQI configuration.



(d) QH configuration.

Figure 3.7: Load-displacement curves under pull-through loading conditions for the different configurations.

For the failure envelope, the data points from the pull-through test were selected in a similar way to the bearing test. The points were chosen at the moment the load begins to decrease, which corresponds to the first peak of the curve. These values represent the average for each layup and are summarized in the table 3.3.

Table 3.3: Average Pull-Through values in kN for different laminates.

Property	QQI	DDQI	QH	DDH
PT	10.52	10.31	7.81	8.28

3.4 Bearing/Pull-Through

In this section, the testing details of a novel mixed-mode multi-axial experimental rig developed in [Furtado et al. \(2023\)](#). [Furtado et al. \(2023\)](#) are used to obtain the experimental results for the pull through and Bearing combination. This enables the decomposition of the uniaxial input load supplied by a standard universal testing machine into simultaneous, co-acting in-plane and out-of-plane load components.

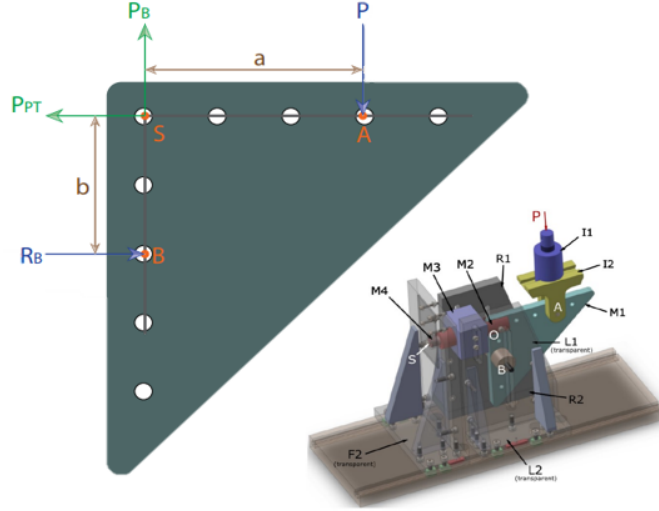


Figure 3.8: Used rig for BEA/PT testing as suggested by ([Furtado et al., 2023](#)).

As described in ([Furtado et al., 2023](#)) and based on Figure 3.8 by applying simple force-balance equilibrium equations along the global horizontal (x) and vertical (y) axes, we obtain the following equations:

$$P_{PT} = R_B \quad (3.1)$$

$$P_B = P \quad (3.2)$$

The localized in-plane bearing load (P_B) remains explicitly known and identically equal to the load P exerted by the testing machine actuator. To evaluate the out-of-plane pull-through load (P_{PT}) as a function of the driving machine force P , the static moment equilibrium about point B is determined as follows:

$$\sum M_B = 0 \iff P_{PT} \cdot b - P \cdot a = 0 \iff P_{PT} = \frac{a}{b} \cdot P \quad (3.3)$$

Knowing both P_B and P_{PT} , the multi-axial load ratio η can be analytically formulated as:

$$\eta = \frac{P_B}{P_B + P_{PT}} = \frac{P}{P + \frac{a}{b}P} = \frac{1}{1 + \frac{a}{b}} \quad (3.4)$$

The domain of the load ratio η is physically bounded between 0 and 1. In a scenario where

$P_B = 0$, a state of pure pull-through is enforced, and η assumes a value of 0. Conversely, if $P_{PT} = 0$, the ratio becomes $\eta = 1$, which corresponds to a pure in-plane bearing loading condition. Under these pure limits, the experimental output is expected to be identical to those obtained via standardized testing procedures detailed in the ASTM D7332 (ASTM International, 2007) and ASTM D5961 (ASTM International, 2018b) standards, respectively. Because the physical dimensions of the lever arms a and b are constrained by the space envelopes of the test fixture, the mixed-mode loading states investigated in (Furtado et al., 2023) were experimentally enforced at discrete η values of 0.2, 0.4, 0.6, and 0.8.

3.4.1 Mechanical Components and Assembly

The rig developed by (Furtado et al., 2023) and illustrated in Figure 3.9 (a) is connected to the universal testing machine Instron 5900R. All structural elements of the multi-axial test fixture were manufactured from high-strength DIN CK45 steel so rigorous lubrication protocols and high surface-finish tolerances were maintained across all mating interfaces to mitigate unwanted frictional forces and prevent inaccurate load cell readings. During the experimental tests, the composite specimen is securely held in position by four peripheral fixation bolts, and a 35 mm diameter clearance opening within the base plate provides the necessary unconstrained volume for the out-of-plane displacement of the bolted connection as shown in Figure 3.9 (b).

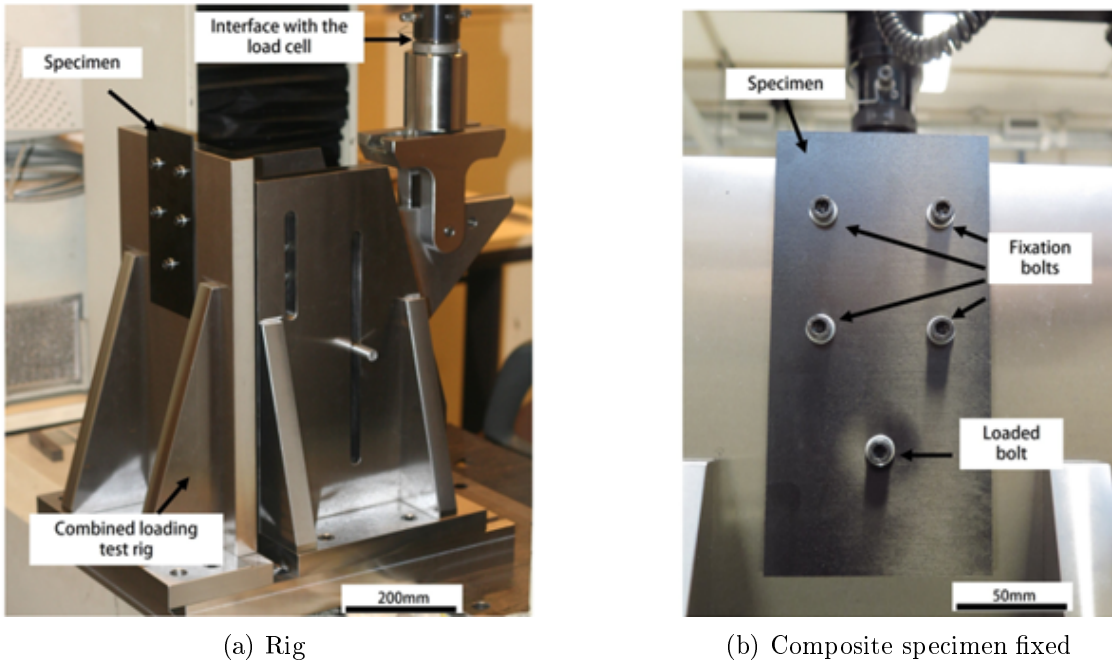
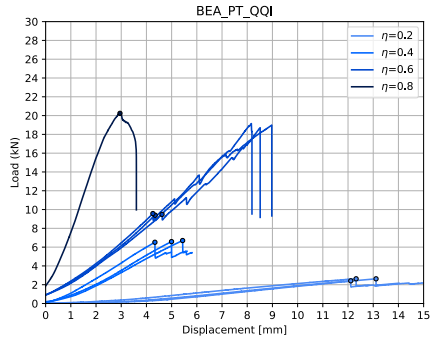


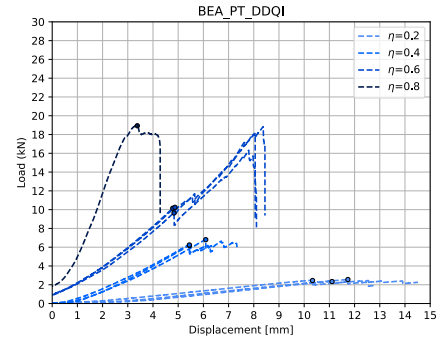
Figure 3.9: Rig and composite specimen (Furtado et al., 2023).

Three tests were performed for each configuration ($\eta = 0.2, 0.4$, and 0.6), with the exception of $\eta = 0.8$, for which only a single test was conducted per layup. Furthermore, no tests were carried out for the DD90 layup. The results for each layup configuration are

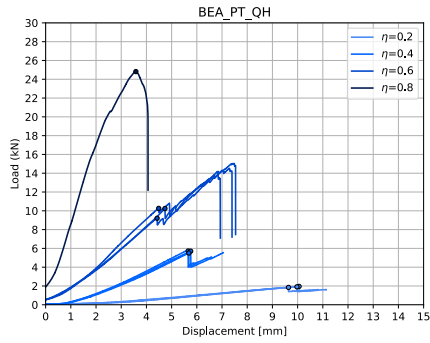
presented in Figure 3.10. The $\eta = 0.8$ configuration consistently triggered fastener failure. The Quad and DD layups exhibited remarkably similar behavior; both their peak loads and the initial load drops in the load-displacement curves occurred at nearly identical load thresholds.



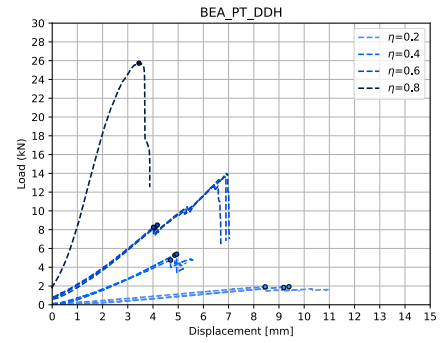
(a) QOI configuration.



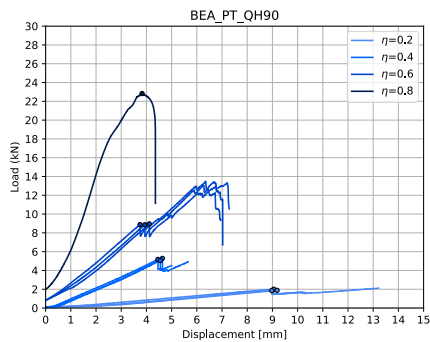
(b) DDQI configuration.



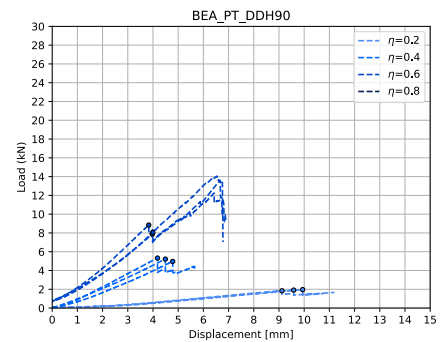
(c) QH configuration.



(d) DDH configuration.



(e) QH90 configuration.



(f) DDH90 configuration.

Figure 3.10: 3D failure interaction envelopes.

Like in the previous tests, the first local load peak value was used to create the failure envelope. This values can be seen in the table 3.4 .

Table 3.4: Average load values in kN used to generate the failure envelope.

Step	QQI	DDQI	QH	DDH	QH90	DDH90
0.2	2.633	2.493	1.910	2.860	1.960	1.860
0.4	6.603	6.417	5.677	5.173	5.173	5.173
0.6	9.480	10.010	9.903	8.313	8.893	8.250
0.8	20.240	18.930	24.820	27.730	22.820	—

3.5 Single-Lap Shear Bolted Joint Testing

3.5.1 Experimental Methodology and Setup

The single-lap shear bolted test is a widely utilized experimental methodology for assessing the mechanical performance of fastened joints in fiber reinforced composite laminates. Because advanced composite materials exhibit significant anisotropy and high susceptibility to localized damage, this specific configuration is essential to understand the intricate load transfer mechanisms across the joint, the contact interactions between the bolt shank and the laminate interface, and the sequential evolution of distinct failure mechanisms under quasi-static loading ([ASTM International, 2018b](#)). In this specific configuration, two composite coupons are overlapped and connected via a single structural bolt. The eccentricity inherent to the single-lap arrangement introduces secondary bending moments, which drastically influence both the initial joint stiffness and the ultimate failure behavior. The assembled joint is carefully aligned and mounted into the mechanical grips of a universal testing machine to ensure that the applied tensile force strictly follows the longitudinal axis of the specimen as shown in Figure 3.11.

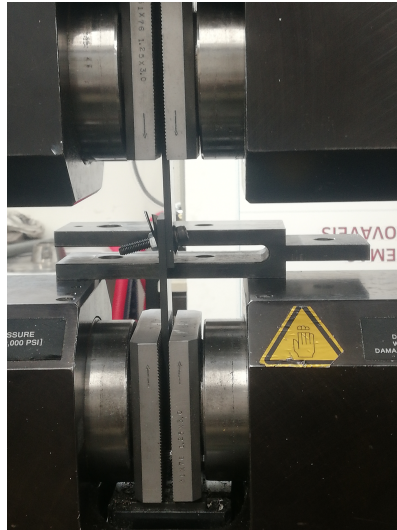


Figure 3.11: Experimental test setup for the single lap shear configuration, showing the composite joint gripped by hydraulic jaws of the INSTRON universal testing machine.

The experimental campaign is conducted under quasi-static tensile loading, typically at a constant cross-head displacement rate. For the present study, the testing procedure was adapted from the ASTM D5961 standard, specifically following the guidelines of Procedure A ([ASTM International, 2018b](#)). However, targeted modifications were implemented to meet specific research constraints, which included altering the baseline specimen dimensions and omitting the use of end tabs.

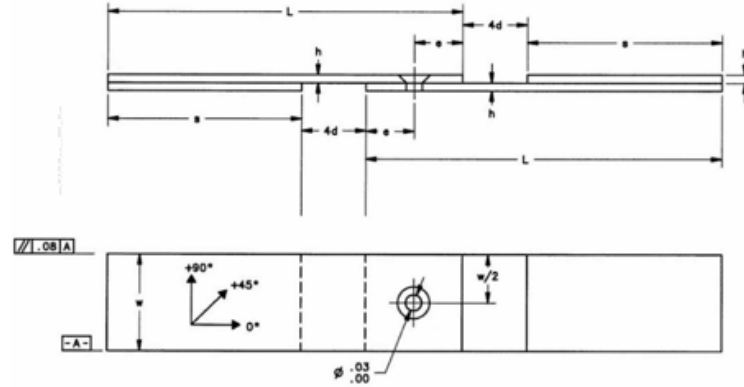


Figure 3.12: Single-Lap shear norm sketch ([ASTM International, 2018b](#))

Three configurations were made , a single lap shear with one bolt, two bolts and three bolts in tandem along the specimen's length. The first case the dimension are exactly the ones in Figure 3.12 with an M6 bolt. For the remaining cases 36 mm were added (equivalent to six times the diameter of the bolt) between each additional bolt the final dimensions can be seen in the Figure 3.13.

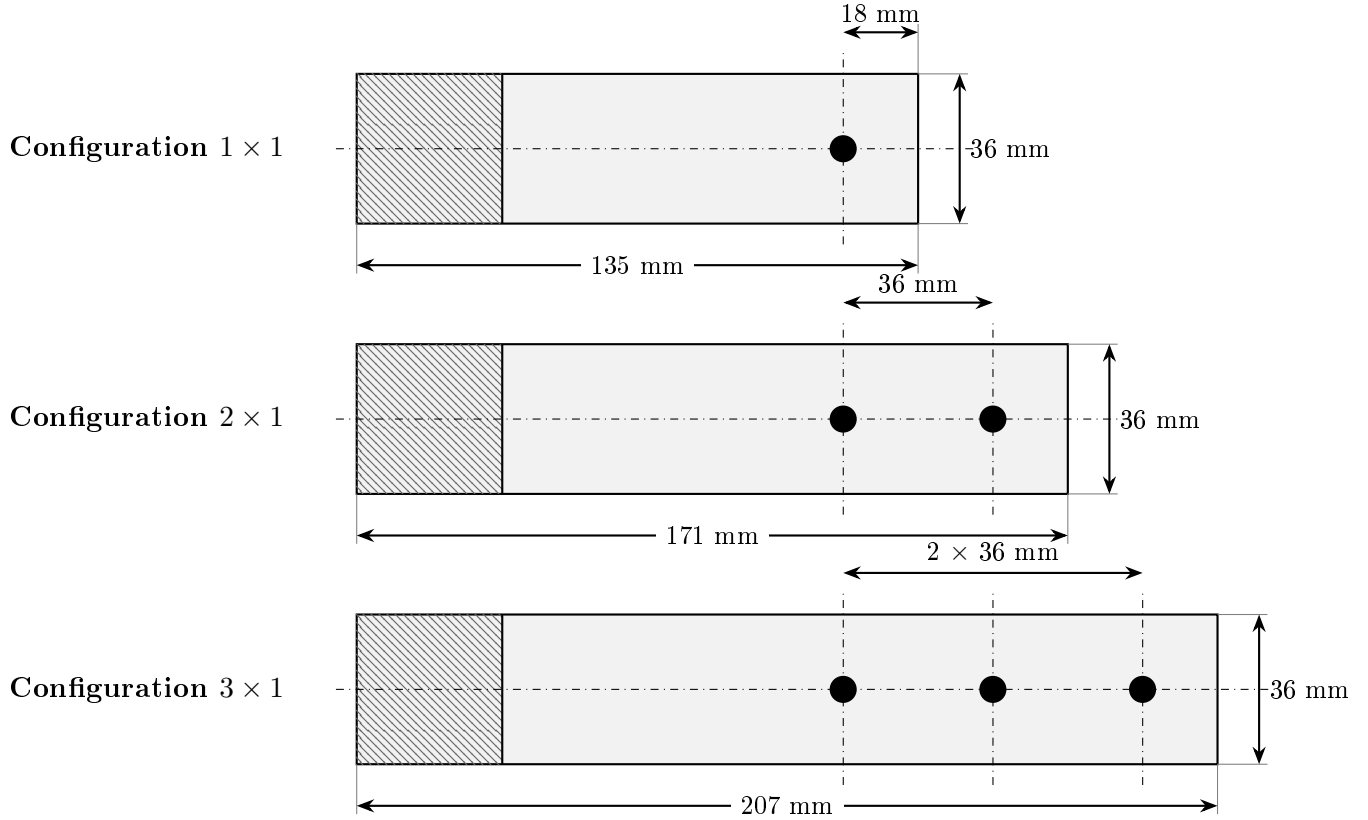


Figure 3.13: Geometric configurations of single-lap shear joint specimens.



(a) 1x1 configuration.

(b) 2x1 configuration.

(c) 3x1 configuration.

Figure 3.14: Overview of the different configurations tested.

3.5.2 Comparison between Failure Mechanisms

By comparing the mechanical response under the same experimental setup for the one-bolt quasi-isotropic layup (Figure 3.15), three highly similar load-displacement curves are observed. Although these curves exhibit a consistent initial behavior, they diverge significantly in their terminal stages, indicating distinct final failure mechanisms. This happens because a single-lap shear bolted joint can fail by various failure mechanisms. The slightest difference in the specimen, such as thickness, length, or even minor fiber inconsistencies, can alter the failure mode.

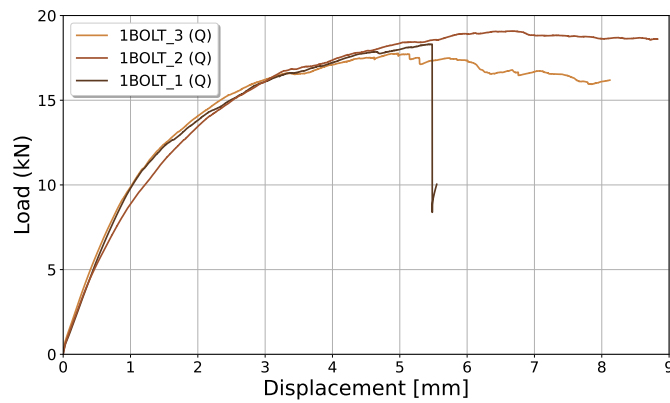


Figure 3.15: Force-Displacement (1 Bolt QOI).

In the graph shown in Figure 3.15, it can be observed that the 1Bolt_1 specimen undergoes a net tension failure mode. Upon reaching the peak load, the curve exhibits a sharp, sudden drop to zero, indicating an abrupt loss of structural integrity. This behavior is typical of net tension, a catastrophic failure mechanism characterized by the sudden tensile fracturing of the laminate along the cross-section of the bolt hole where the stress concentration is highest (Camanho and Matthews, 1997). Unlike the gradual energy dissipation seen in bearing modes, net tension results in a rapid macro-crack propagation through the net-section width. The post mortem joint is shown in Figure 3.16.



Figure 3.16: Failure analysis for specimen 1Bolt_1.

The second load-displacement 1Bolt_2 curve is characteristic of progressive bearing. After reaching the peak load, the load value decreases slightly but subsequently maintains a more or less constant plateau throughout the remainder of the test, as shown in Figure 3.15. This progressive bearing behavior is characterized by the continuous, localized crushing of the composite matrix and fibers directly in front of the bolt shank (ASTM International, 2018b). Instead of a sudden catastrophic collapse, the material yields gradually, absorbing energy through micro-damage accumulation such as localized delamination, fiber breakage,

and matrix yielding as the displacement increases ([Camanho and Matthews, 1997](#)). This phenomenon can be observed in Figure 3.17, where the bolt tearing through the laminate can be clearly seen from below.



Figure 3.17: Failure analysis for specimen 1Bolt_2.

The third case, observed in the 1Bolt_3 specimen, exhibits a combined failure mode consisting of bearing coupled with delamination. This behavior is evidenced in Figure 3.15, where the curve starts to decrease smoothly after reaching its peak. This gradual drop in load capacity indicates that the laminate is progressively losing its stiffness due to the initiation and propagation of interlaminar cracks, commonly known as delamination. In composite joints, out-of-plane stresses induced by bolt bending and mechanical interaction often trigger these secondary delamination mechanisms, which severely compromise the structural integrity of the surrounding plies [Camanho and Matthews \(1997\)](#). The delamination is clearly visible in Figure 3.18 side view.



Figure 3.18: Failure analysis for specimen 1Bolt_3.

3.5.3 Comparison between stacking sequences

One information that can be taken from this research is to evaluate the mechanical performance of Double-Double (DD) laminates relative to conventional Standard Quad Quasi-Isotropic (QI). The DD architecture is fundamentally designed to mimic the homogenized elastic properties of a Quasi-Isotropic (QI) laminate as described per Tsai (Tsai and Melo, 2014) and explained in Chapter 2. As observed in the experimental data, the DDQI response curve remarkably tracks the baseline QQI curve. Both configurations achieve a peak load within the range of approximately 19–20 kN as shown in Figure 3.19 and exhibit a nearly identical initial joint stiffness. This strong correlation confirms that the DD layups sequences successfully replicates standard Quad configuration. Regarding damage tolerance, while the conventional QQI reaches a slightly higher absolute peak load, the DDQI demonstrates highly stable post peak behavior.

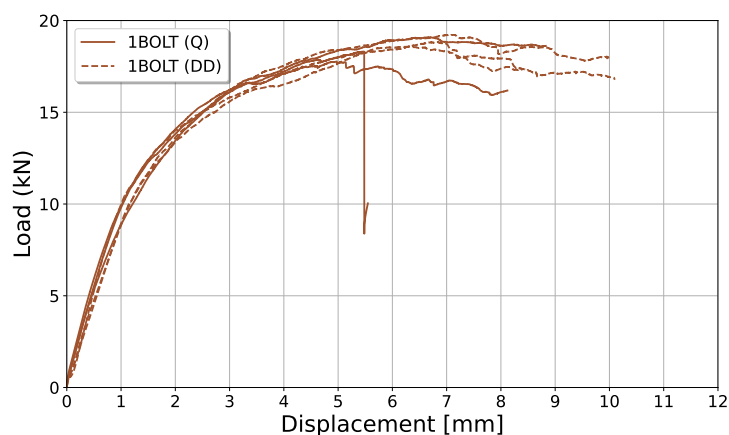


Figure 3.19: Force-displacement for QI layups

The mechanical comparison between the standard Hard (QH) and the Double-Double Hard (DDH) configurations further validates the architectural concept. Both load curves stabilize at a distinct bearing plateau of approximately 15 kN as in Figure 3.20. From a micro-mechanics perspective, standard Hard laminates are highly susceptible to sudden longitudinal splitting because the blocked 0° fibers lack sufficient transverse backing support. Consequently, the DDH configuration maintains its bearing plateau over a wider displacement stroke, indicating that the homogenized nature of the DD enhances the localized ductility of the bearing zone, preventing the discrete, abrupt damage steps seen in standard Hard configurations (McCarthy and McCarthy, 2003; Tsai, 2021).

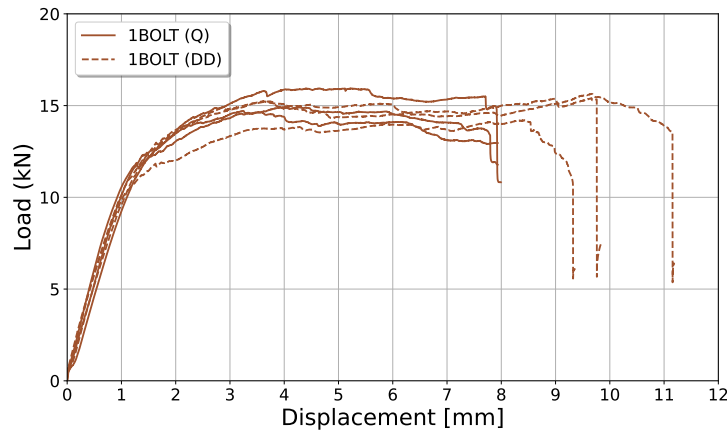


Figure 3.20: Force-displacement for H layups

When both layup sequence are rotated by 90° , the stiffness mimicry is preserved, but the joint strength falls drastically to a common lower bound of approximately 13 kN as shown in Figure 3.21. Whether evaluating a standard Quad or a DD layup sequence, the lower percentage of 0° fibers or the complete absence of 0° fibers along the loading direction triggers a transition to a net tension failure mode in all cases. Because this failure mechanism is governed by the transverse tensile strength of the individual plies and the polymer matrix ([Chang and Chang, 1987](#)), the layup sequence benefits of the DD approach (homogenization and dispersion) are completely overshadowed by the lack of longitudinal reinforcement. Both the QH90 and DDH90 joints fail catastrophically showing that no stacking sequence refinement can compensate for a matrix-dominated loading path.

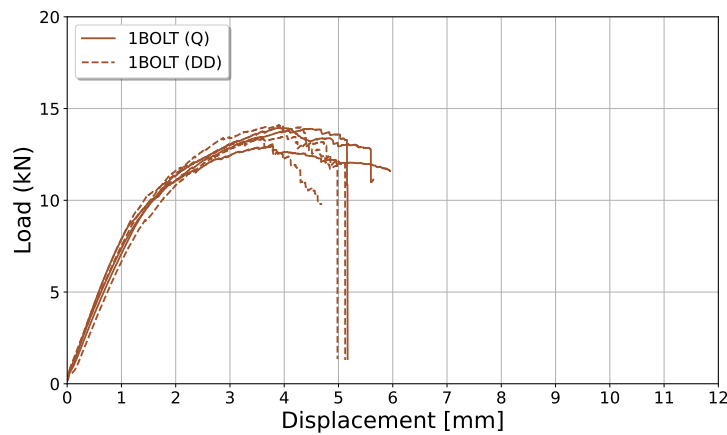
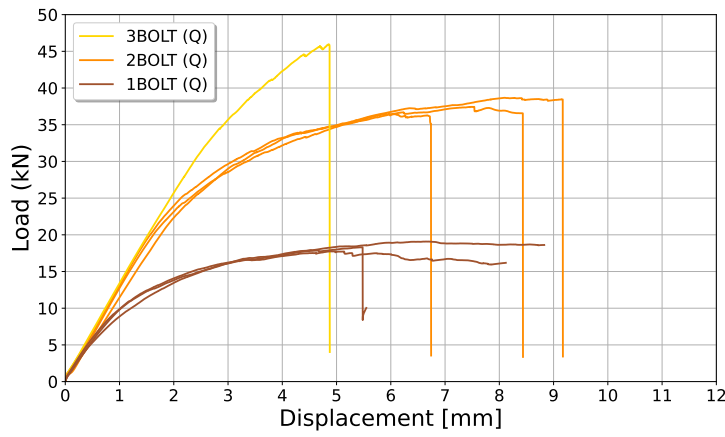


Figure 3.21: Force-Displacement for H90 layups

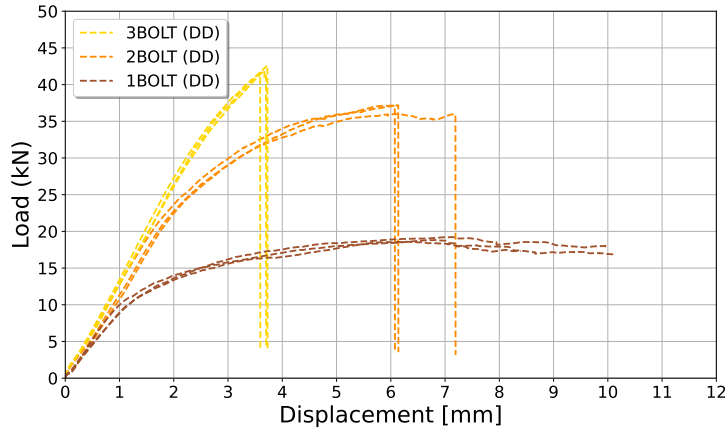
3.5.4 Comparison Between Joint Configurations

The global mechanical performance of composite single-lap shear (SLS) joints is fundamentally governed by a highly non-linear scalability when transitioning from a single-bolt baseline (1×1) to multi-bolt arrays (2×1 and 3×1). The structural response is not a simple linear summation of individual fastener capacities, but rather progressive phenomenon dictated by load-sharing efficiency, localized stress concentration fields, and geometric notch sensitivity (Hart-Smith, 1986). This evolution can be analyzed by tracking the progressive performance shifts across the 1×1 , 2×1 , and 3×1 configurations, where the laminate orientation and stacking layout change the load redistribution capacity (Camanho and Matthews, 1997). However, the reason the 2×1 configuration achieves highly progressive curves with extended plateaus reaching $\approx 30\text{--}35$ kN for QQI, DDQI, QH, and DDH is that the highly stressed outermost bolt row undergoes localized micro-crushing and matrix yielding upon reaching its bearing threshold. This localized damage induces a temporary relaxation in local stiffness, which acts as a progressive strain-softening mechanism that allows the excess load to successfully shed and redistribute onto the remaining interior bolt (Thoppul et al., 2009). In the matrix-dominated QH90 and DDH90 laminates, this beneficial load-shedding is completely absent; because they cannot undergo localized plastic bearing deformation, the severe stress concentration around the 2×1 array yields instantaneous crack propagation, forcing a brittle net-section tension failure (Chang and Chang, 1987). A critical structural threshold is crossed when transitioning from the 2×1 configuration to the 3×1 multi-bolt array. While the 3×1 configuration reaches the highest absolute peak loads across all fiber-dominated layups ($\approx 42\text{--}46$ kN), its post-peak behavior undergoes a complete change, entirely losing the progressive ductile plateaus seen in the 1×1 and 2×1 counterparts and terminating in an instantaneous catastrophic drop to near-zero capacity except the Hard laminates, because of the zero degrees plies. The physical reason why the joint becomes drastically more prone to a brittle net-section tension failure when moving from two to three bolts is the progressive geometric restriction of the net-section ligament area along the transverse cross-section of the plate (McCarthy et al., 2005a). While a single or double bolt configuration permits localized bearing plastic deformation without compromising the global ligament, the introduction of three consecutive holes causes the massive stress concentration fields generated by each closely spaced bolt to interact, overlap, and coalesce (McCarthy et al., 2002). Combined with the severe out-of-plane secondary bending and peeling stresses inherent to single-lap shear joints, the net-section ligament surrounding the highly loaded end-bolt cannot sustain the combined strain field once peak bearing capacity is reached (Ireman, 2001). The comparison between Quad-ply and Double-Double architectures further highlights how internal stacking sequences influence this multi-bolt scalability and damage progression. By eliminating the large 45° angular jumps typical of conventional sequences, the homogenized DD architecture minimizes interlaminar shear stress discontinuities, preventing premature delaminations from coalescing between adjacent fastener holes under the severe stress fields

of multi-bolt arrays (Melo et al., 2017). However, as shown in Figure 3.25, these architectural refinements reach a strict performance ceiling under matrix-dominated loading scenarios (Tsai, 2013). In the QH90 and DDH90 configurations, transitioning from 1×1 to 2×1 and 3×1 fails to induce any progressive scalability or bearing behavior, with all configurations failing via pure, brittle net-section tension. Because these layups lack longitudinal fibers to bridge cracks, the severe net-section area reduction of the 3×1 geometry triggers an immediate, low-energy brittle failure that plateaus poorly between 24–27 kN, proving that fiber orientation remains the primary determinant of joint safety, completely overshadowing both fastener density and stacking homogenization when loading occurs in critical, non-reinforced directions (Chang and Chang, 1987).

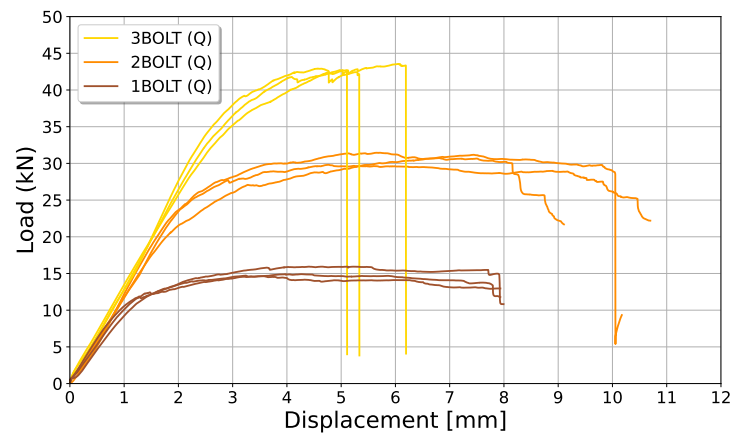


(a) QQI configuration.

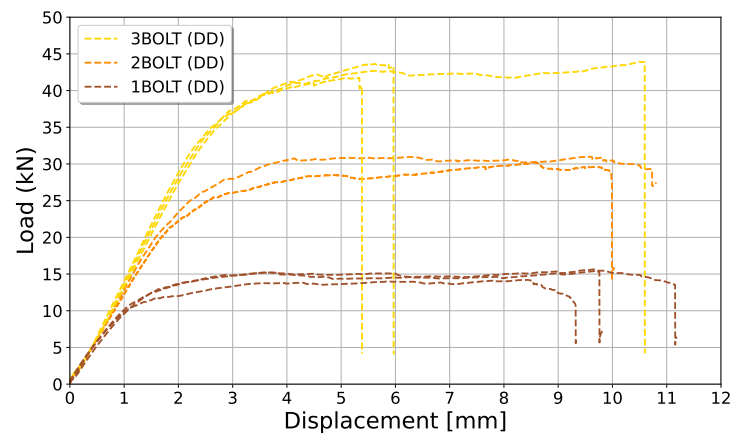


(b) DDQI configuration.

Figure 3.22: Force-displacement curves (*1Bolt*, *2Bolt*, and *3Bolt*) for the QQI and DDQI laminate configurations.



(a) QH configuration.



(b) DDH configuration.

Figure 3.23: Force-displacement curves (*1Bolt*, *2Bolt*, and *3Bolt*) for the QH and DDH laminate configurations.

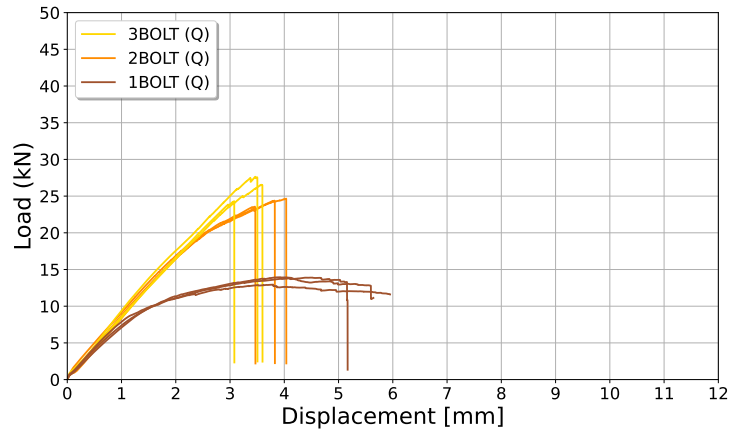


(a) Top view of the damaged area

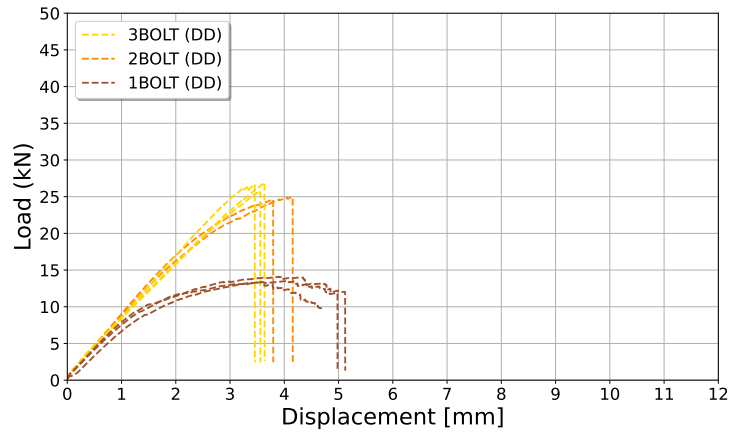


(b) Side view showing delamination

Figure 3.24: 2x1QH, progressing bearing followed by delamination.



(a) QH90 configuration.



(b) DDH90 configuration.

Figure 3.25: Force-displacement curves (*1Bolt*, *2Bolt*, and *3Bolt*) for the QH90 and DDH90 laminate configurations.



(a) Top view of the specimen



(b) Side view of the bolted joint

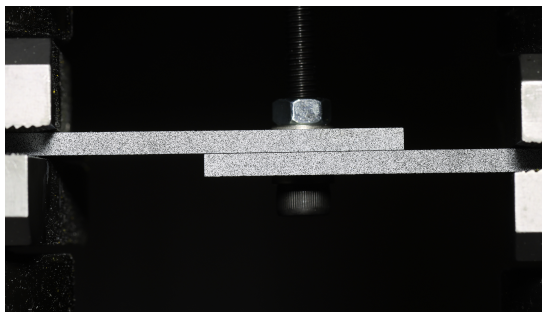


(c) Side view

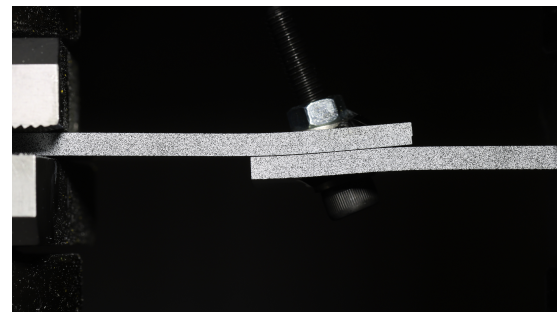
Figure 3.26: 3x1QH90, net-tension failure

3.5.5 Digital Image Correlation Analysis

To establish a rigorous comparison between the experimental results and the forthcoming computational predictions, as well as to properly calibrate the numerical methods detailed in the next chapter, experimental tests equipped with Digital Image Correlation (DIC) were performed for nearly every laminate layup. DIC is a non-contact, full-field optical technique used to measure surface displacements and map strain distributions during mechanical testing. The method operates by tracking the deformation of a high-contrast random speckle pattern previously applied to the specimen's surface as shown on Figure 3.27. By capturing successive digital images throughout the loading history, cross-correlation algorithms mathematically track the displacement of small pixel subsets, allowing for the precise computation of the full-field strain tensor. From the DIC is taken the specimen displacement and then correlated via time with the Loads from each experimental test. The resulting Force-Displacement curves can be observed in Figures 3.28 to 3.30. As expected the displacement in this graphics is lower than the displacement measured directly in the experimental curve because it doesn't show the machine's influence.

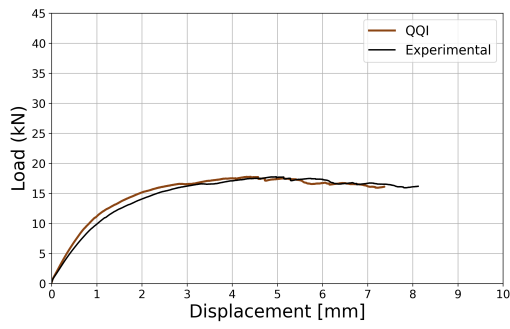


(a) Initial state of the DIC analysis

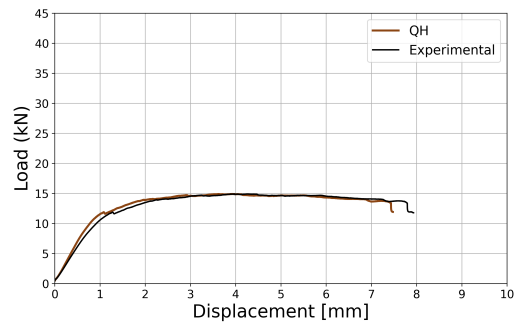


(b) Final state of the DIC analysis

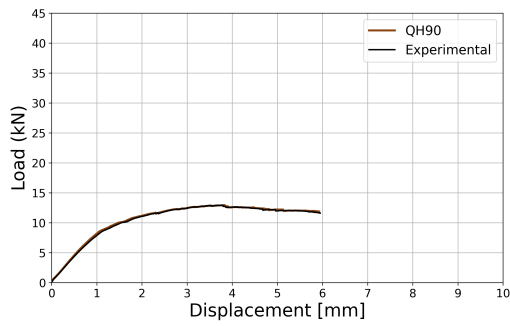
Figure 3.27: Side view of the single-lap shear joint test 1x1 QOI, showing the comparison between the (a) initial and (b) final stages of the DIC capture, highlighting the secondary bending and joint rotation.



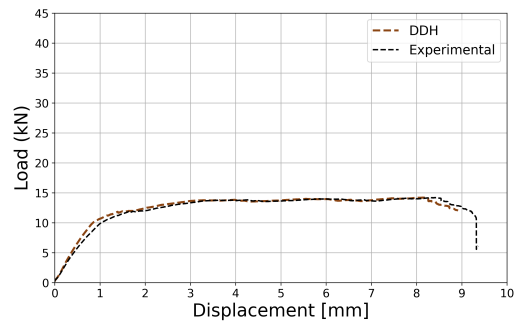
(a) DIC 1x1 QQI.



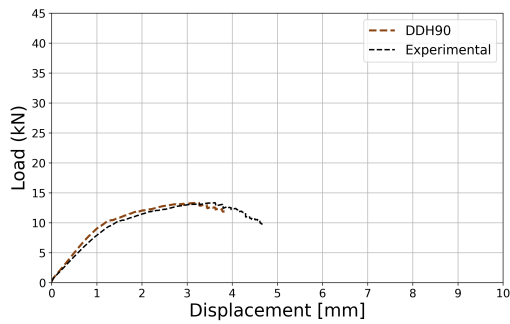
(b) DIC 1x1 QH.



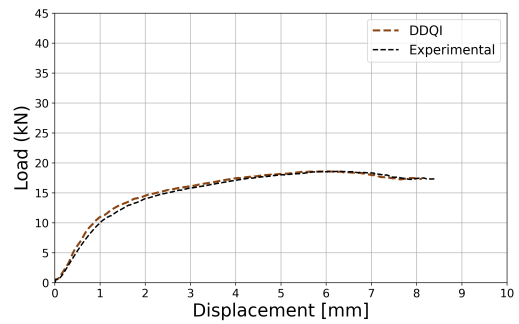
(c) DIC 1x1 QH90.



(d) DIC 1x1 DDH.

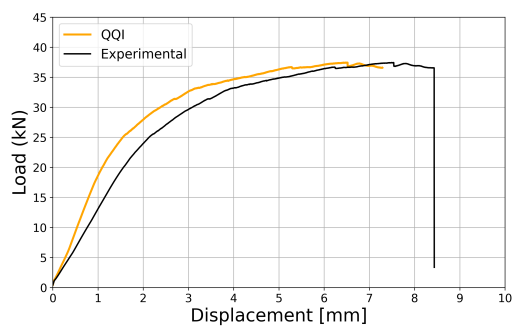


(e) DIC 1x1 DDH90.

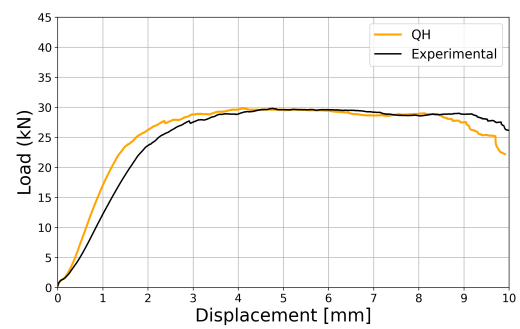


(f) DIC 1x1 DDQI.

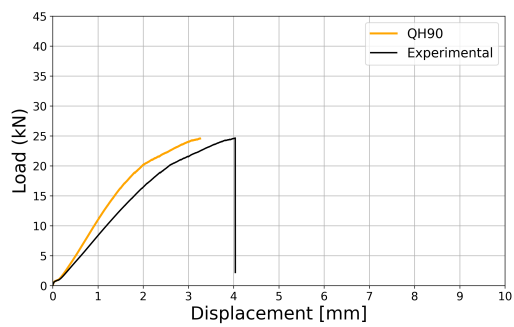
Figure 3.28: Force-Displacement DIC 1x1 laminates.



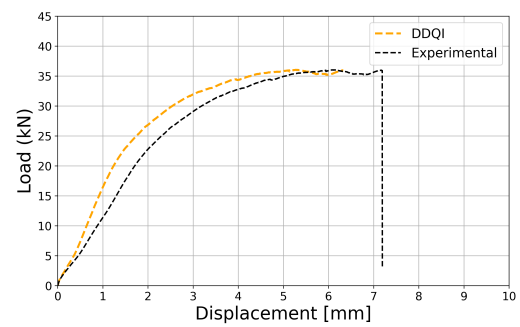
(a) DIC 2x1 QOI.



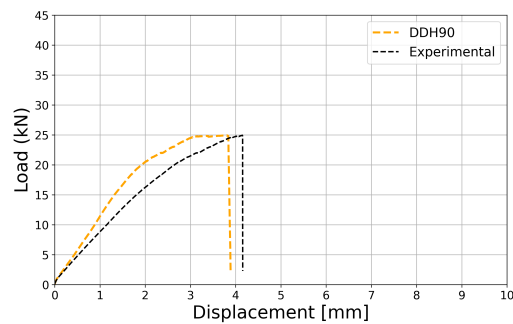
(b) DIC 2x1 QH.



(c) DIC 2x1 QH90.

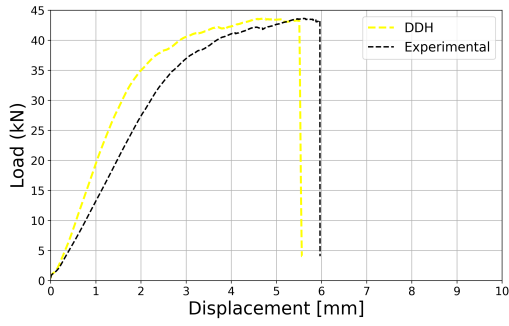


(d) DIC 2x1 DDQI.

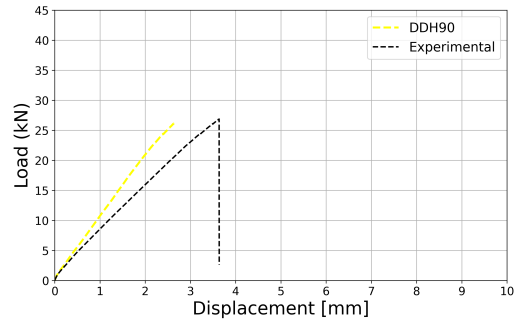


(e) DIC 2x1 DDH90.

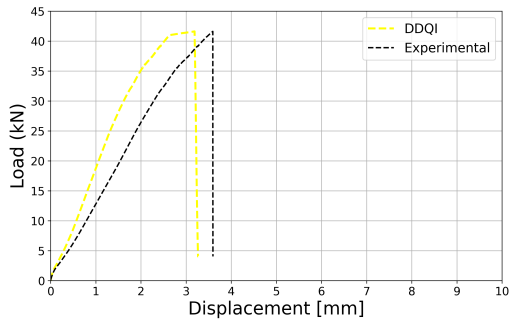
Figure 3.29: Force-Displacement DIC 2x1 laminates.



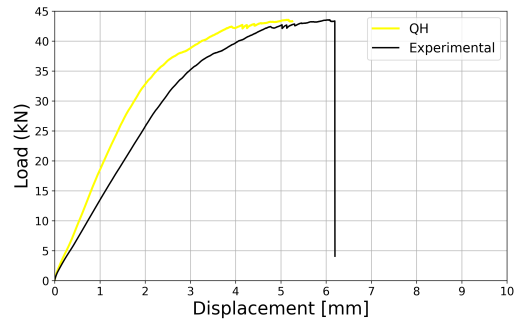
(a) DIC 3x1 DDH.



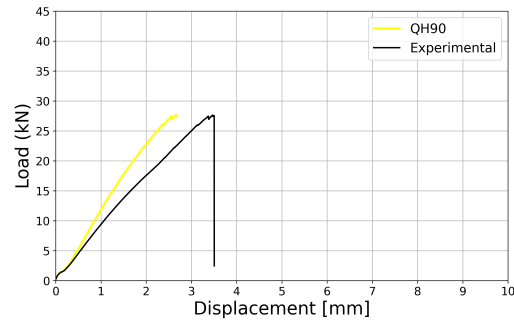
(b) DIC 3x1 DDH90.



(c) DIC 3x1 DDQL.



(d) DIC 3x1 QH.



(e) DIC 3x1 QH90.

Figure 3.30: Force-Displacement DIC 3x1 laminates.

3.6 Failure Envelope Generation

Due to their inherently anisotropic nature, oriented laminates exhibit distinct bearing resistance values depending on the in-plane shear loading direction, displaying a considerably greater stiffness and strength along their primary axis. Therefore, is need a 3D failure envelopes to characterize them, as mentioned in Volpi et al. (2026) To generate the failure envelopes bearing and pull through test and the combination between both for indices $\eta = 0.2, 0.4, 0.6$, and 0.8) Table 3.5 summarizes all the values used for the determination of failure envelope for each laminate.

To generate the failure interaction envelopes, several structural assumptions were implemented. For the quasi-isotropic (QI) configurations, the bearing strengths at off-axis angles of 90° , 60° , and 30° were assumed to be identical to the baseline longitudinal bearing (BEA) value due to their tailored isotropic in-plane behavior. Regarding the multi-axial loading ratios (η), the experimental values obtained for the configurations rotated by 90° match those evaluated at 0° . For the hard configurations, the parameters corresponding to the (η) 90° rotation are explicitly presented under the QH90 and DDH90 columns in Table 3.5.

Table 3.5: Summary of experimental values for different laminate configurations in kN.

Property	QQI	DDQI	QH	DDH	QH90	DDH90
BEA	21.720	21.230	18.810	17.470	—	—
BEA90	—	—	13.830	15.970	—	—
BEA60	—	—	17.330	16.630	—	—
BEA30	—	—	16.560	16.520	—	—
0.2	2.633	2.493	1.910	2.860	1.960	1.860
0.4	6.603	6.417	5.677	5.173	5.173	5.173
0.6	9.480	10.010	9.903	8.313	8.893	8.250
0.8	20.240	18.930	24.820	27.730	22.820	—
PT	10.520	10.310	7.810	8.280	—	—

As observed in the first two failure envelopes presented in Figure 3.31, derived from Equations (3.5) and (3.6), the DDQI response curve remarkably tracks the baseline QQI curve with only a slightest difference between them. This close agreement confirms that the DD layup sequences successfully replicate the standard Q configuration, providing a highly accurate approximation.

When comparing the failure envelopes of the QH and DDH configurations, a notable discrepancy in the transverse bearing response becomes evident. This difference is primarily attributed to the absence of experimental data points for the DDH90 configuration during the envelope generation. Both failure envelopes are illustrated in Figure 3.32, with their respective analytical behaviors governed by Equations (3.7) and (3.8). The failure

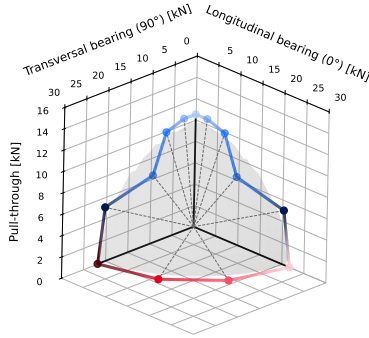
interaction envelopes for the QH90 and DDH90 configurations, which will be used on computational models in Chapter 4, correspond to the QH and DDH failure envelopes with a transposition between the longitudinal and transverse bearing components respectively.

$$\left(\frac{f_{TB}}{21.63}\right)^2 + \left(\frac{f_{PT}}{10.7}\right)^2 + \left(\frac{f_{LB}}{21.63}\right)^2 = 1 \quad (3.5)$$

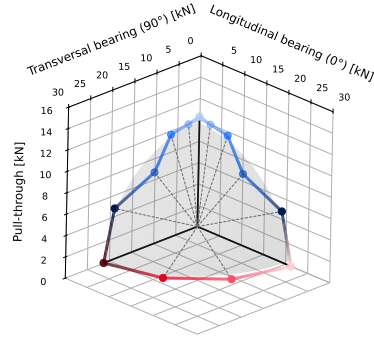
$$\left(\frac{f_{TB}}{21.01}\right)^2 + \left(\frac{f_{PT}}{9.82}\right)^2 + \left(\frac{f_{LB}}{21.01}\right)^2 = 1 \quad (3.6)$$

$$\left(\frac{f_{TB}}{20.47}\right)^2 + \left(\frac{f_{PT}}{7.32}\right)^2 + \left(\frac{f_{LB}}{23.71}\right)^2 = 1 \quad (3.7)$$

$$\left(\frac{f_{TB}}{24.43}\right)^2 + \left(\frac{f_{PT}}{6.98}\right)^2 + \left(\frac{f_{LB}}{24.50}\right)^2 = 1 \quad (3.8)$$

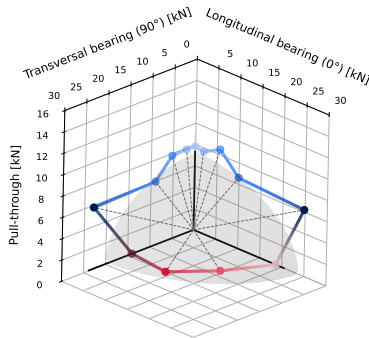


(a) QQI laminate, Equation (3.5)

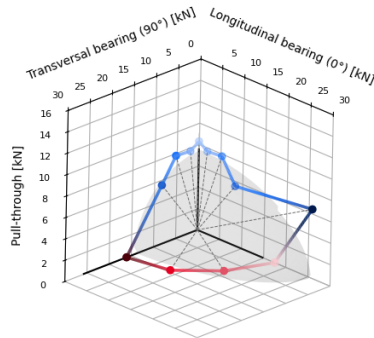


(b) DDQI laminate, Equation (3.6)

Figure 3.31: 3D failure interaction envelopes mapping Transversal Bearing (f_{TB}), Longitudinal Bearing (f_{LB}), and Pull-through (f_{PT}) forces for different configurations.



(a) QH laminate, Equation (3.7)



(b) DDH laminate, Equation (3.8)

Figure 3.32: 3D failure interaction envelopes mapping Transversal Bearing (f_{TB}), Longitudinal Bearing (f_{LB}), and Pull-through (f_{PT}) forces for QH and DDH configurations.

3.6.1 Concluding Remarks

The formulated 3D failure envelopes provide a mathematically consistent and computationally efficient tool to predict the onset of failure under complex bearing and pull-through loading regimes. For QQI and DDQI, the analytical formulation yields excellent approximations, capturing the in-plane isotropic behavior.

For QH and DDH, despite the high local sensitivity introduced by highly multi-axial ratios like $\eta = 0.8$, the proposed equations serve as robust boundaries for structural failure criteria. Geometrically, refining these interaction surfaces by omitting the $\eta = 0.8$, allows the boundary curve to converge towards a more mathematically accurate ellipsoidal approximation. By preventing the artificial local expansion or swelling of the domain caused by this point, the formulation recovers a smoother, tighter ellipsoidal path. Consequently, this geometric adjustment yields more conservative failure limits, eliminating overoptimistic strength evaluations in the multi-axial domain and ensuring safer numerical predictions.

Chapter 4

Computational Simulations

This chapter presents the numerical framework developed to predict the mechanical response and first non-linearity onset of the multi-bolt composite joints under multi-axial loading states. To avoid the computational cost associated with high-fidelity 3D finite element models, an simplified modeling strategy based using Abaqus Cartesian connector elements is implemented. The structural behavior is evaluated through three distinct calibration methodologies: starting with purely elastic calibration (Method 1), a combined elastoplastic formulation calibrated via experimental bearing test data (Method 2), and a direct experimental calibration using single-lap shear from digital image correlation (DIC) curves (Method 3). These numerical formulations are validated against the experimental responses across the 1x1, 2x1, and 3x1 multi-bolt arrays. Furthermore, the calculated connector force outputs are coupled with the analytical 3D failure interaction envelopes detailed in Chapter 3 to identify the onset of structural non-linearity and to track the evolution of the multi-axial bearing/pull-through loading ratio (η) throughout the deformation history.

4.1 Simplified Connector Models

High computational expenses constitute a major disadvantage of high-fidelity 3D numerical analyses. Despite their predictive accuracy, the geometric complexity and dense mesh requirements of these models render them unsuitable for large-scale structural configurations or multi-fastener air frame simulations. To significantly reduce computation time while preserving global accuracy, a simplified bolt-modeling strategy was proposed for the implementation of fasteners in multi-bolt joint configurations [Volpi \(2024\)](#). In this approach, Abaqus defined connector elements are employed to represent the individual bolts.

The main objective of these simplified models, when used in conjunction with an failure criterion derived from an ellipsoidal failure envelope, is to establish an efficient framework for predicting multi-bolt joint degradation without the prohibitive runtime of fully discretized 3D finite element models [Volpi \(2024\)](#). The elastic properties of the composite used in the simulations are presented in the Table [4.1](#).

Table 4.1: Elastic properties and mass density of the composite material.

Property	Nomenclature	Value
Longitudinal Elastic Modulus (MPa)	E_1	115100
Transverse Elastic Modulus (MPa)	E_2	9080
Out-of-Plane Elastic Modulus (MPa)	E_3	9080
Poisson's Ratio (1-2)	ν_{12}	0.310
Poisson's Ratio (1-3)	ν_{13}	0.310
Poisson's Ratio (2-3)	ν_{23}	0.487
In-Plane Shear Modulus (MPa)	G_{12}	5100
Out-of-Plane Shear Modulus (MPa)	G_{13}	5100
Out-of-Plane Shear Modulus (MPa)	G_{23}	3053
Mass Density (g/mm ³)	ρ	1.58×10^{-9}

4.1.1 Numerical Modeling via Cartesian Connector Elements

The numerical idealization of the physical fasteners is implemented through `CONN3D2` Cartesian connector elements within the Abaqus environment. These elements provide a phenomenological representation of the complex bolt laminate interaction, governed by a multi-stage constitutive law that replaces the physical contact interactions with equivalent mathematical formulations (Dassault Systèmes, 2024).

4.1.1.1 Kinematic Constraints and Degree of Freedom Decoupling

The Cartesian connector establishes a local coordinate system at the respective fastener location, enabling the independent definition of translational responses along the U_1 , U_2 , and U_3 directions. According to the *Abaqus Analysis User's Guide*, this kinematic decoupling is critical to accurately isolate the in-plane shear-bearing response along the loading axis while maintaining independent structural constraints in the out-of-plane directions (Dassault Systèmes, 2024).

To ensure a realistic and objective load distribution across the laminate, the connector endpoints are linked to the internal periphery of the fastener hole via distributing couplings. This kinematics constraint formulation distributes the force transmission over a predefined group of nodes, thereby preventing artificial, non-physical stress concentrations at a single node. Furthermore, it enables the effective transmission of the structural moments induced by the secondary bending effect inherent to single-lap shear geometries (Dassault Systèmes, 2024).

4.1.1.2 Constitutive Behavior: Elasticity and Plasticity

The connector section utilizes a coupled Elastic-Plastic constitutive framework to replicate the nonlinear load-displacement response observed experimentally:

- **Nonlinear Elasticity:** A tabular force-displacement relationship is defined to replicate the initial contact stabilization. This allows the joint stiffness to evolve dynamically as a function of the relative displacement, following the standard Abaqus Connector Elasticity framework (Dassault Systèmes, 2024).
- **Connector Plasticity:** To simulate the localized progressive crushing of the polymer matrix and the associated fiber buckling that characterizes bearing failure, the Abaqus Connector Plasticity feature is invoked. This feature is defined by an empirical yield force threshold and a hardening table that maps the connector force against the accumulated plastic relative displacement (F vs. u_{pl}) (Dassault Systèmes, 2024). *It is worth noting that, from a physical standpoint, the experimental failure mechanism in composite laminates is governed by micro-mechanical progressive damage and fragmentation rather than true irreversible plastic deformation. However, the plasticity framework is adopted here as an equivalent phenomenological approach, as its mathematical formulation is going to be used to capture the extensive, stable load-displacement plateaus observed in the experiments without introducing the numerical instabilities typically associated with complex softening damage laws.*

The simplified geometries were based on the numerical modeling parameters and geometric scale outlined in the work of Volpi (Volpi, 2024), where in this case the dimensions and the composite layup were adapted to match the specific experimental tests.

4.1.2 Pure Bearing

The simplified pure bearing model is composed of a $36 \times 135 \text{ mm}^2$ composite laminate and two $36 \times 36 \times 6 \text{ mm}^3$ steel plates, following the geometry established in the low-fidelity framework from (Volpi, 2024). All parts are modeled with $1 \times 1 \text{ mm}^2$ four node linear elastic shell elements with reduced integration (S4R), resulting in a total of 6603 elements. The outer edge of the composite laminate is clamped, while a 1 mm displacement selected to exceed the maximum displacement achieved by the high-fidelity analyses is applied to both steel blocks in the direction of the joint. Surface-to-surface contact was defined between the overlapping section of the laminates with a Hard contact normal behavior. Abaqus-defined connector elements are used to attach each steel plate to the composite laminate by means of a circular region of influence, defined as the area of the washer used in the high fidelity models to accurately predict the clamping performance. The simulation model is presented in the Figure 4.1

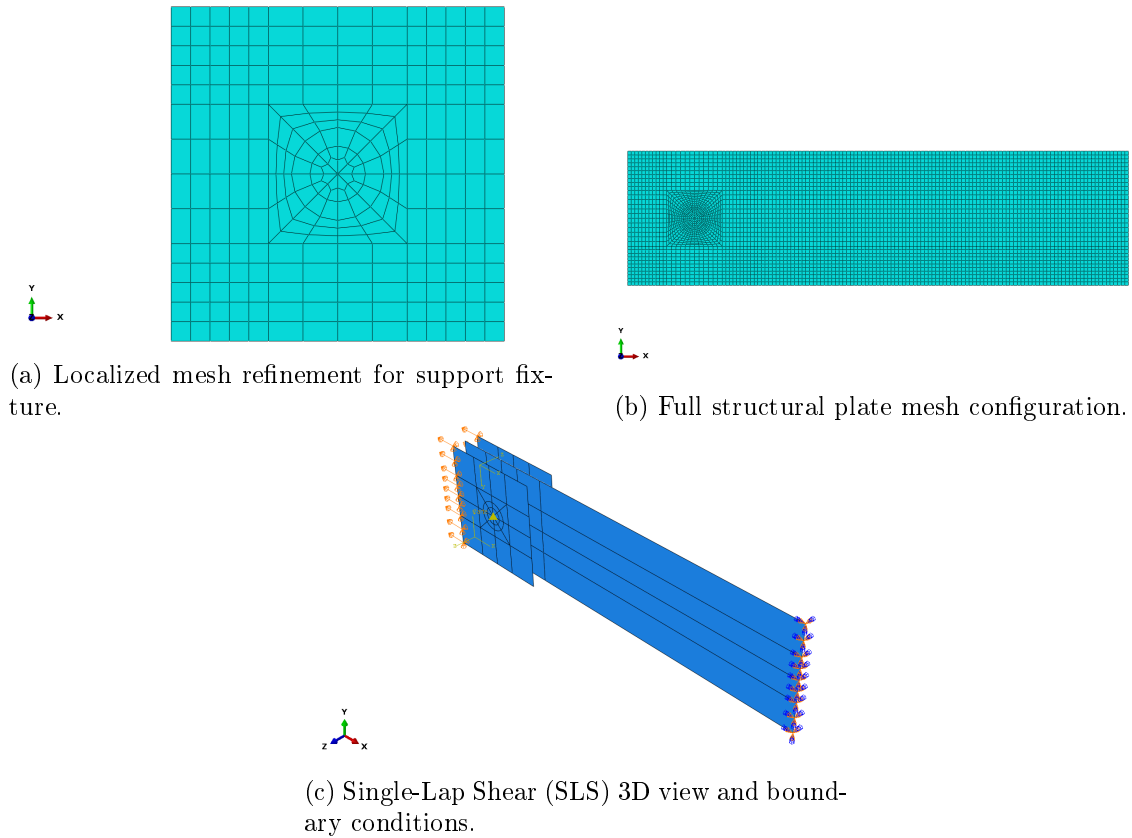


Figure 4.1: Three-dimensional finite element mesh configurations, boundary conditions, and localized refinement details for both single-bolt bearing and single-lap shear numerical models.

4.1.3 Pure Pull-Through

The simplified model for the simulation of pure pull-through conditions consists of an analytical rigid shell rig with a diameter 35 mm clearance hole and a $84 \times 84 \text{ mm}^2$ shell laminate modeled with 7372 S4R elements. The rig is clamped in place to avoid unwanted movement, while the composite plate is constrained at the midpoint of each edge to avoid the rotation of the specimen along the z -axis and its translation in the x - and y -directions. An out-of-plane displacement of 1 mm was applied to the free edge of the connector element along its axis (Volpi, 2024). The simulation model is presented in Figure 4.2.

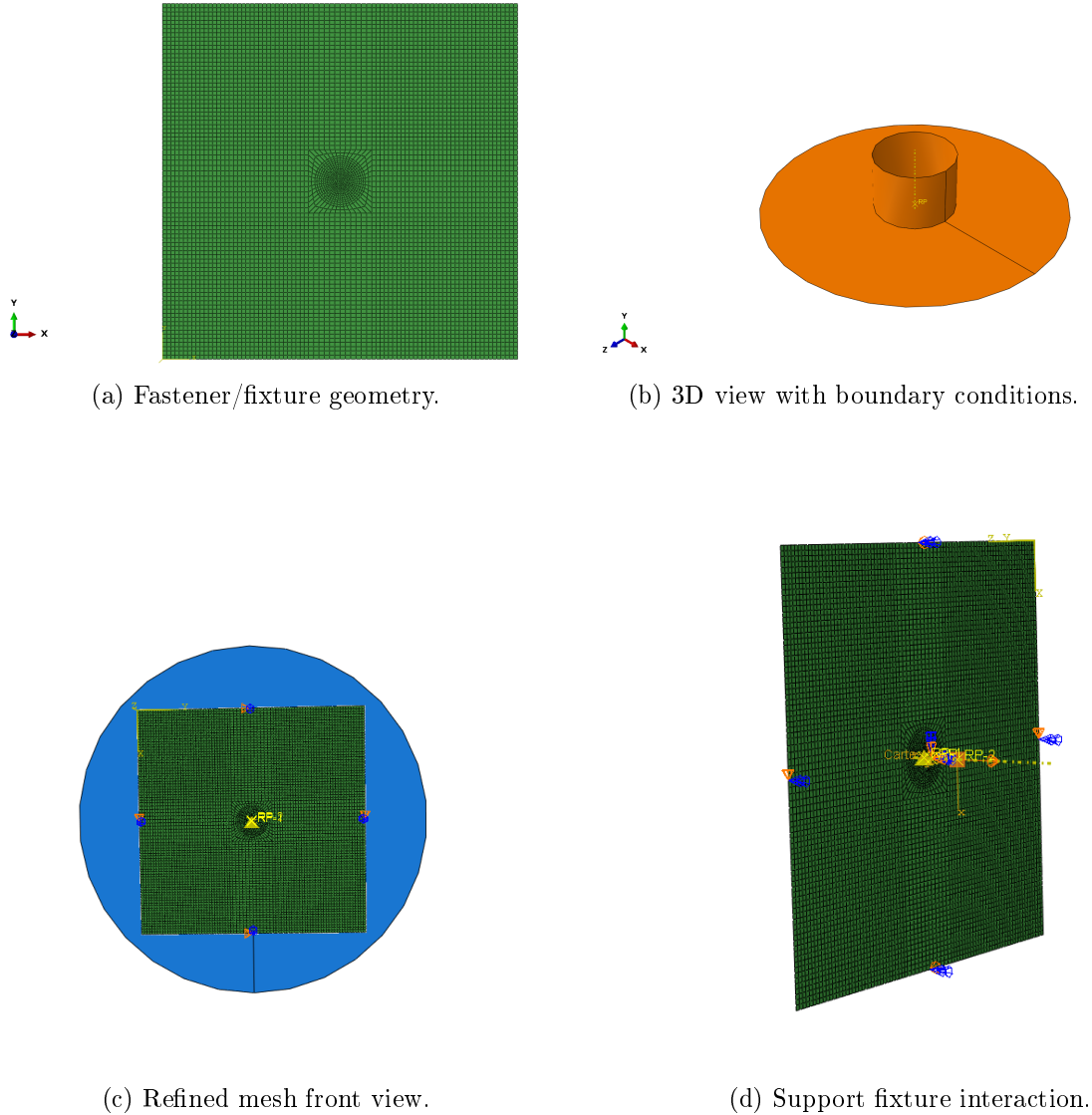


Figure 4.2: Three-dimensional finite element mesh configurations, boundary conditions, and geometric definitions of the single-bolt testing specimen components.

4.1.4 Single-Lap Shear

The simplified multi-bolt laminates are constituted by two composite laminates arranged in the aforementioned multi-bolt configurations, and modeled as planar shells simulated with 8624 to 18248 linear elastic S4R elements. A fine mesh was adopted to maximize accuracy, the model dimensions are exactly the same used on the specimens for the experimental test. The models used in each configuration are presented in Figures ?? to 4.5.

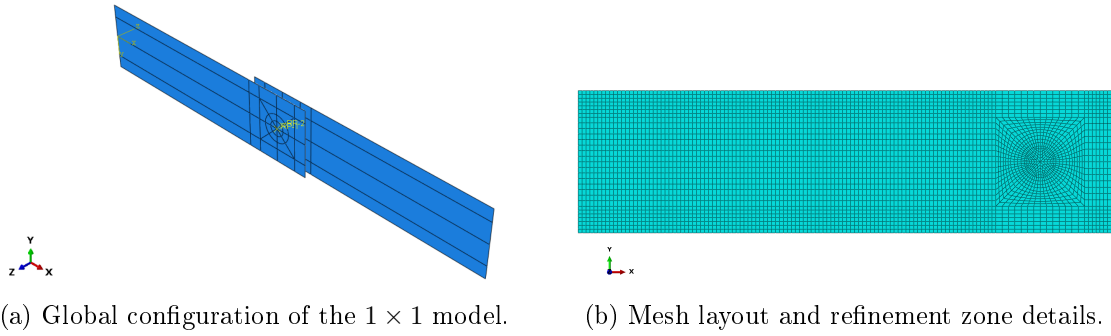


Figure 4.3: Numerical finite element configurations for the 1×1 single-bolt simulation setup.

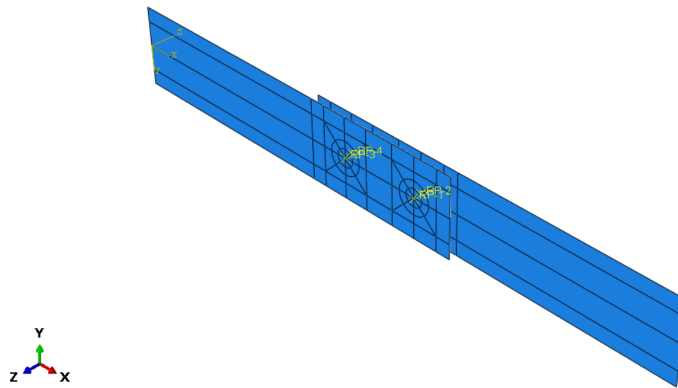


Figure 4.4: Model 2x1

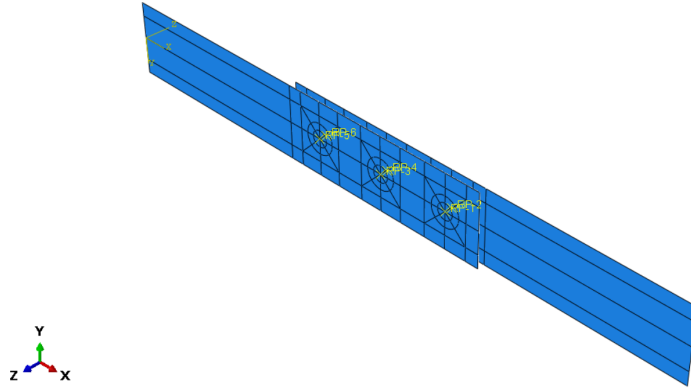


Figure 4.5: Model 3x1

To represent the experimental test the boundary condition type used is the Displacement/Rotation which allow to fix one side in this case the left side not allowing displacement in any direction, and on the right its also fixed in two directions y and to simulate the machine movement its used a displacement in x .

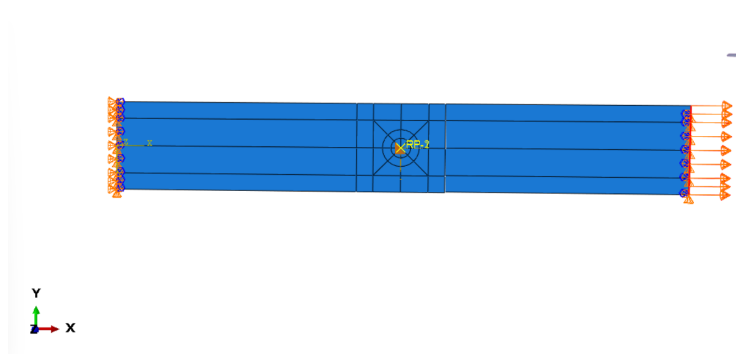


Figure 4.6: Boundary conditions

To better represent the interaction between the laminates, an interaction created an interaction between the contacting area of both laminates. A Finite Sliding tracking formulation is employed alongside a Surface to surface discretization method to precisely account for potential relative displacements and stress concentrations at the interface during loading. The normal behavior of this interface utilizes a "Hard" Contact pressure-overclosure relationship. This mathematical formulation prevents any non-physical interpenetration between the shell elements of the upper and lower laminates, ensuring that compressive normal forces are strictly transmitted upon contact. Crucially, the "Allow separation after contact" condition is enforced.

To simulate the mechanical fasteners without the high computational cost of full 3D solid bolt models, a simplified discrete connector approach was implemented based on the framework established by Santos (Volpi, 2024). This methodology relies on a combination of kinematic coupling constraints and discrete wire elements to transfer loads realistically

between the overlapping laminates. As illustrated in Figure 4.7, the fastener implementation begins by defining a dedicated Reference Point (RP) at the geometric center of the bolt hole for each individual shell laminate (designated as RP-1 and RP-2 across the joint interface). To distribute the concentrated forces from the fastener smoothly into the surrounding composite structure, a Kinematic Coupling constraint is assigned to each reference point, Figure 4.7.

This constraint couples the control RP to the peripheral surface nodes defining the hole boundary (s_Surf LO 1-1) that represents the washer radius. By locking all six degrees of freedom (U1, U2, U3, UR1, UR2, UR3), the region within the specified influence radius is forced to deform as a rigid body. Physically, this area of influence prevents the localized collapse or distortion of the shell mesh around the perimeter, replicating the strict boundary constraint imposed by a physical, rigid bolt shank pressing against the laminate hole wall during bearing loading. The structural interaction and load transfer between the two distinct laminates are then achieved by bridging the two localized reference points (RP-1 and RP-2) with a localized wire feature assigned a Cartesian Connector section.

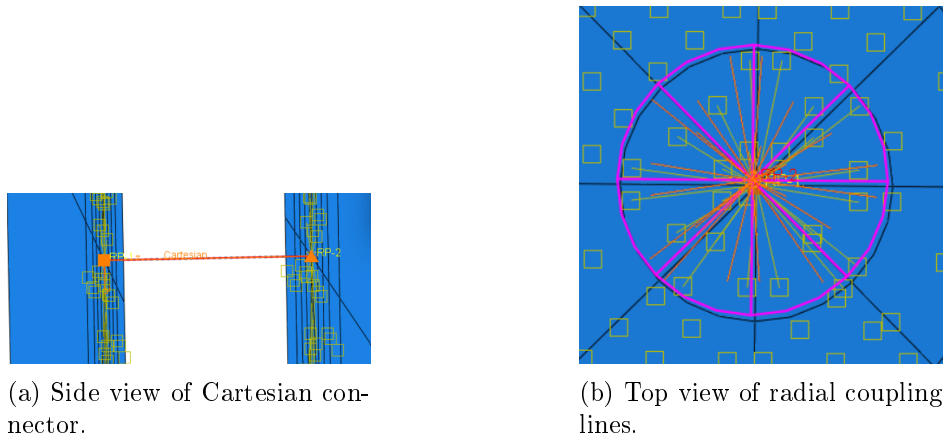


Figure 4.7: Numerical modeling details of the joint kinematics:(a) side view of the single-lap assembly showing the Cartesian connector wire bridging adjacent reference points ($RP - 1$ and $RP - 2$), and (b) top view of the radial distribution lines loading the hole boundary mesh.

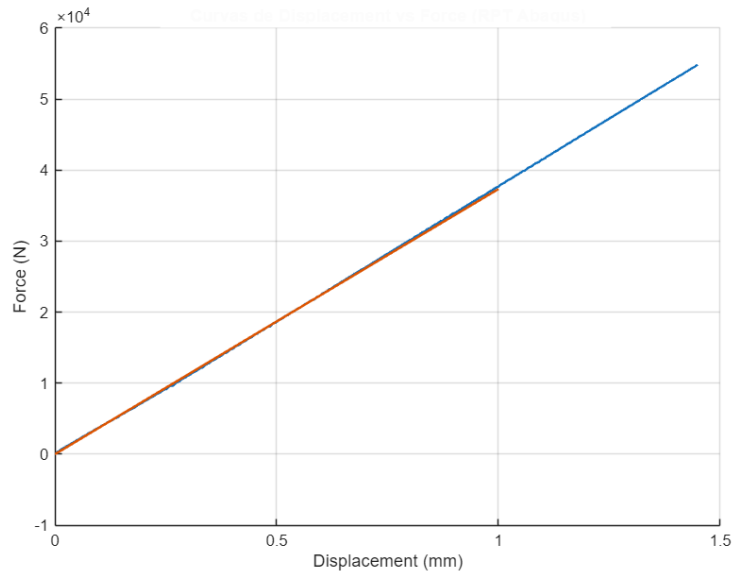
4.2 Method 1-Elastic calibration

4.2.1 Connector stiffness calibration

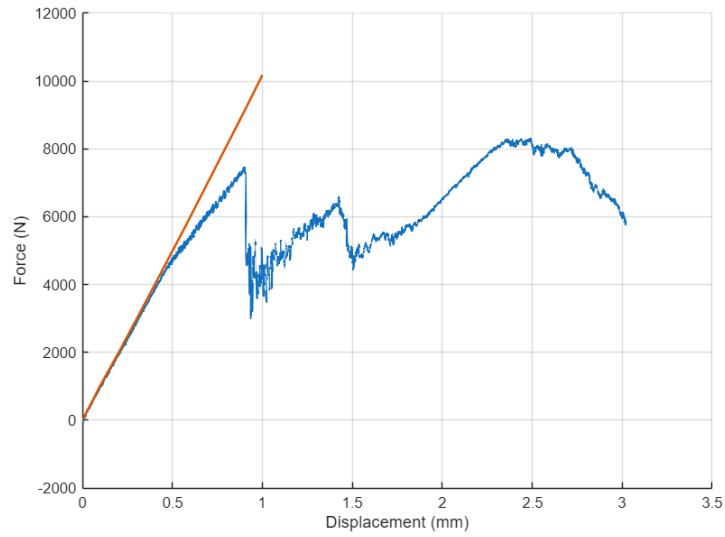
Abaqus-defined connector elements require an *a priori* determination of the connector stiffness. Although this parameter can be estimated through various empirical connector flexibility formulations, the most adequate expression for different laminates and joint geometries is complex to define analytically given the significant disparity between structural formulas (Volpi, 2024). Therefore, to ensure the highest possible degree of numerical accuracy, the connector stiffness was obtained through an iterative calibration methodology using the optimization protocol adjusted by Volpi (2024) and Santos (2025), until the linear slopes of the simplified pure bearing and pure pull-through match the one of the high fidelity simulation for pure bearing and pure pull-through. The resulting connector stiffness values corresponding to pure bearing and pure pull-through for each layup can be seen in Table 4.2. Each layup sequence will use their respective calibrated stiffness on their simplified simulations. The stiffness values for the Quad layup sequence and its Hard counterpart have similar results across both tests, where the most notable discrepancy is found within the bearing stiffness, with a difference of approximately 2000 N/mm being observed.

Table 4.2: Connector stiffness values for BEA and Pull-Through.

Laminate	PT (N/mm)	BEA (N/mm)
QQI	32000	75000
DDQI	32000	77000
QH	27000	66500
DDH	26000	66000
QH90	27000	80000
DDH90	26000	80000



(a) BEA calibration, where the blue curve represents load-displacement curve in elastic regime from high-fidelity 3D finite element models, and the red curve corresponds to the simplified connector model



(b) Pull-through stiffness calibration, where the blue curve represents load-displacement curve from high-fidelity 3D finite element models, and the red curve corresponds to the simplified connector model.

Figure 4.8: Stiffness calibration for both tests.

4.2.2 Results

Following the calibration of the models, the load-displacement curves for each connector direction CTF1, CTF2, and CTF3 are generated through Abaqus. These response curves are then intersected with the failure envelope presented in Chapter 3, where CTF1 represents the longitudinal force (longitudinal bearing), CTF2 represents the transverse force (transverse bearing), and CTF3 represents the out-of-plane force (pull-through). The point resulting from this intersection corresponds to the initiation of the first non-linearity as shown on Figure 4.9.

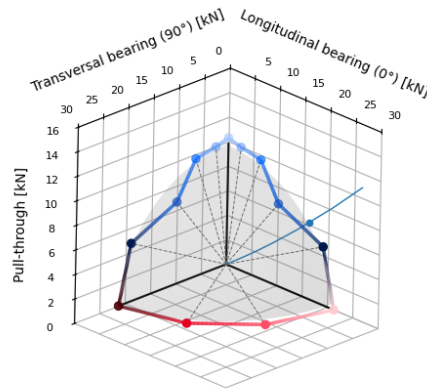


Figure 4.9: Intersection between the combination of CTF1, CTF2 and CTF3 with the QQI failure envelope.

The results obtained for the three configurations are presented in Tables 4.3 to 4.5, which detail the predicted load for the non-linearity (blue point), the experimental load at the same displacement (black point), the percentage error between both values, and the ultimate experimental peak load.

Table 4.3: First non-linearity point for 1x1 configuration.

Laminate	Abaqus Load (kN)	Real Load at Same Disp. (kN)	Error (%)	Highest Load in Real Curve (kN)
QQI	17.015	12.73	33.66	17.770
DDQI	16.480	12.26	34.42	18.580
QH	15.590	11.51	35.45	14.630
DDH	17.010	11.24	51.33	14.210
QH90	14.130	11.70	20.77	12.940
DDH90	14.070	10.09	39.44	13.340

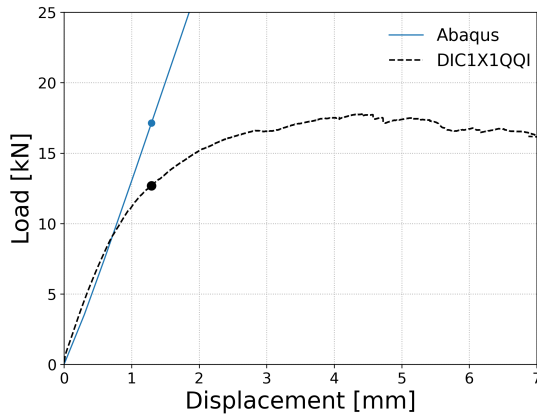
Table 4.4: First non-linearity point for 2x1 configuration.

Laminate	Abaqus Load (kN)	Real Load at Same Disp. (kN)	Error (%)	Highest Load in Real Curve (kN)
QQI	36.010	28.80	25.03	36.480
DDQI	36.300	29.50	23.05	36.030
QH	33.500	24.20	38.43	29.810
DDH	34.300	20.60	66.50	30.160
QH90	29.910	22.10	35.34	23.570
DDH90	28.900	30.20	4.30	24.610

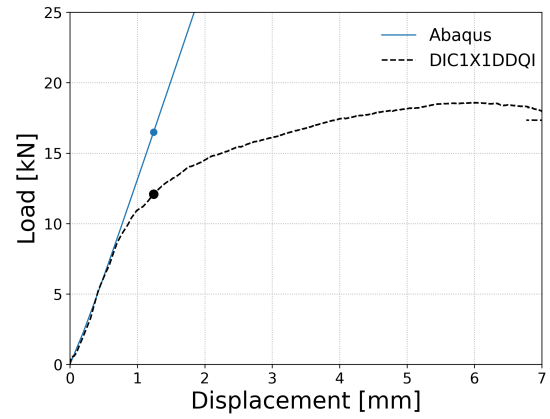
Table 4.5: First non-linearity point for 3x1 configuration.

Laminate	Abaqus Load (kN)	Real Curve Load at Same Disp. (kN)	Error (%)	Highest Load in Real Curve (kN)
QQI	53.000	35.30	50.14	45.950
DDQI	50.090	41.40	20.99	41.950
QH	48.900	34.70	40.92	42.730
DDH	50.500	38.40	31.51	41.780
QH90	41.400	—	—	24.210
DDH90	14.070	10.09	39.44	13.340

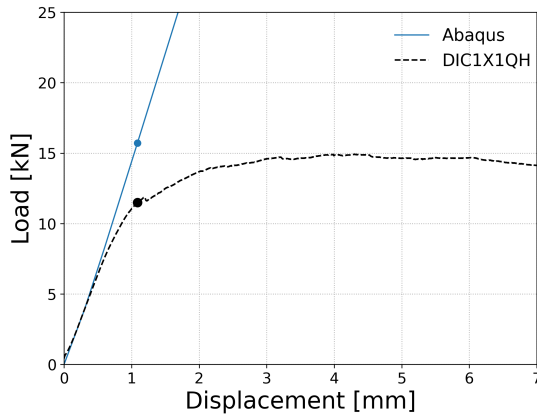
Based on the data presented in Tables 4.3 to 4.5 and corroborated by Figures 4.10, 4.11, and 4.12 with the exception of Figures 4.11d and 4.12a, where the curves present more pronounced stiffness discrepancies due to the absence of load-displacement plots derived from DIC measurement. Can be concluded that this approach, while overestimating the load, has relatively close displacement values to the displacement of the first non linearity. The single lap shear test exhibits extensive plastic deformation, which cannot be accurately captured by a purely elastic model, even though the initial elastic calibration remains highly accurate for most cases. In fact, most of these predicted values lie near the ultimate peak of each force-displacement curve. Consequently, the failure envelope must be adapted to capture the initial non-linear behavior, which requires resolving the constraints discussed in Chapter 3.



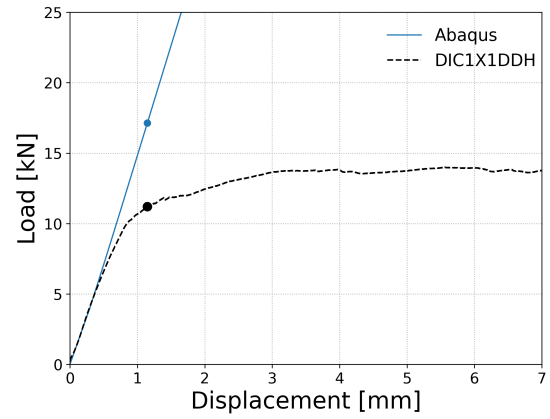
(a) DIC 1x1 QQI elastic region.



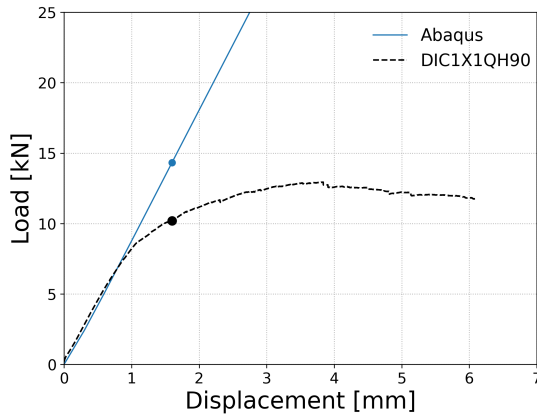
(b) DIC 1x1 DDQI elastic.



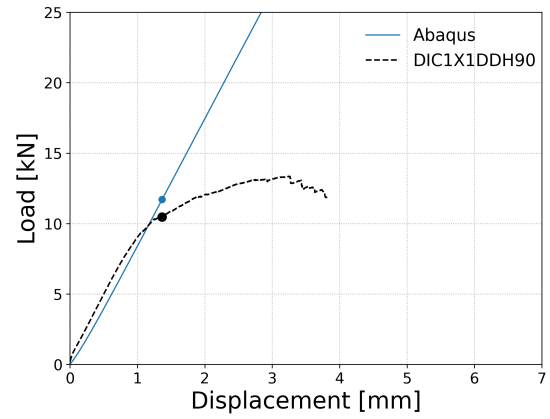
(c) DIC 1x1 QH elastic.



(d) DIC 1x1 DDH elastic.

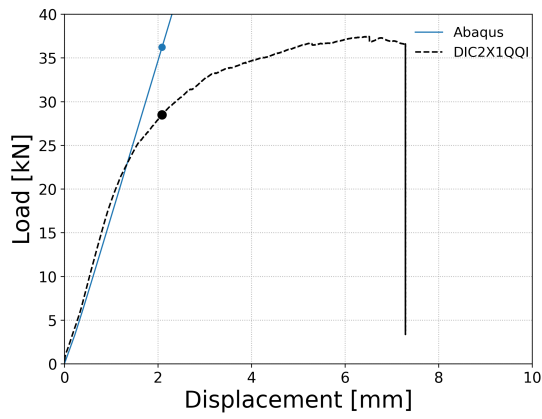


(e) DIC 1x1 QH90 elastic.

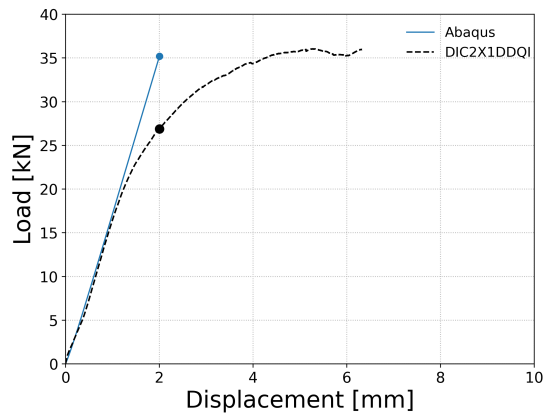


(f) DIC 1x1 DDH90 elastic.

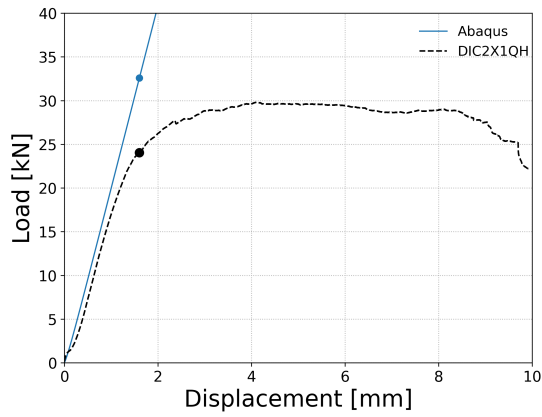
Figure 4.10: Linear elastic curves extracted for the 1×1 configurations compared with experimental curves



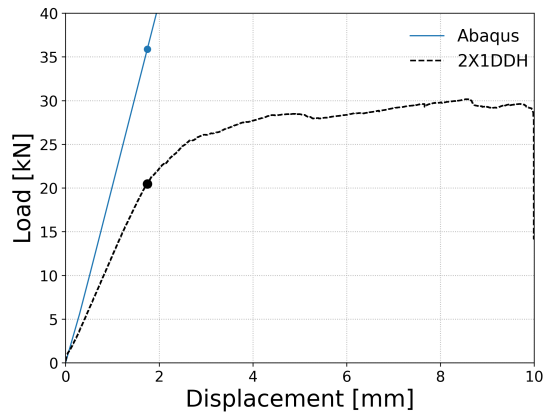
(a) 2x1 QOI elastic.



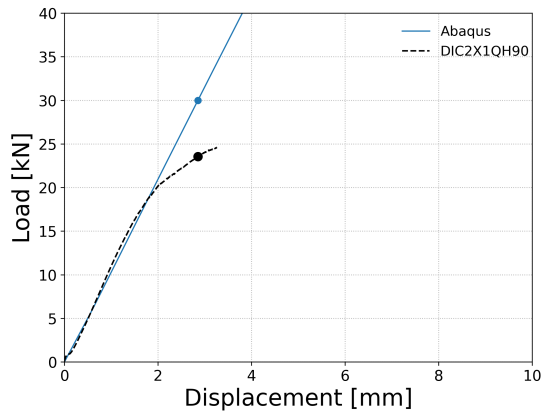
(b) DIC 2x1 DDQI elastic.



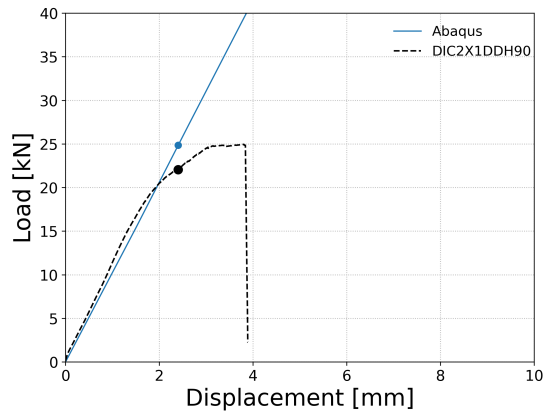
(c) DIC 2x1 QH elastic.



(d) 2x1 DDH elastic.

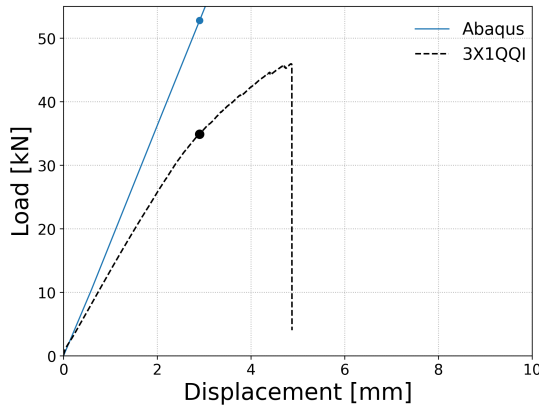


(e) DIC 2x1 QH90 elastic.

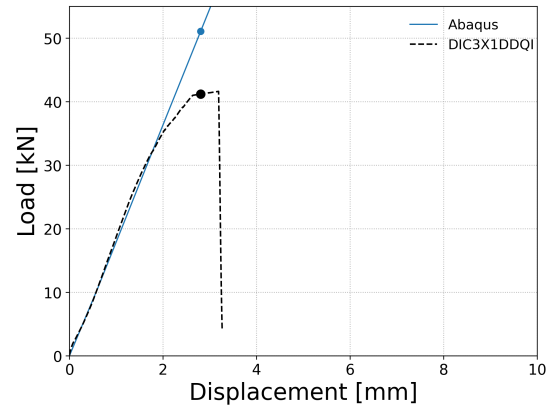


(f) DIC 2x1 DDH90 elastic.

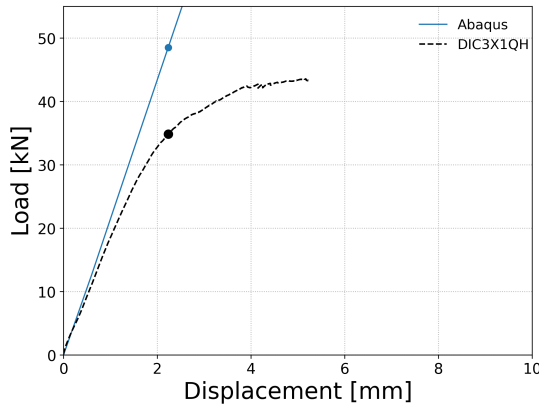
Figure 4.11: Linear elastic curves extracted for the 2×1 configurations compared with experimental curves, note that some plots are not using DIC load-displacement curve .



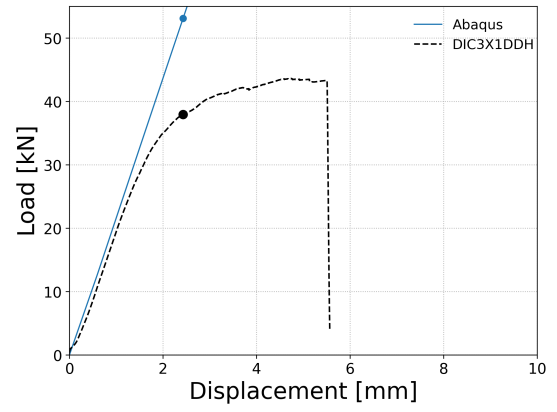
(a) 3x1 QQI elastic.



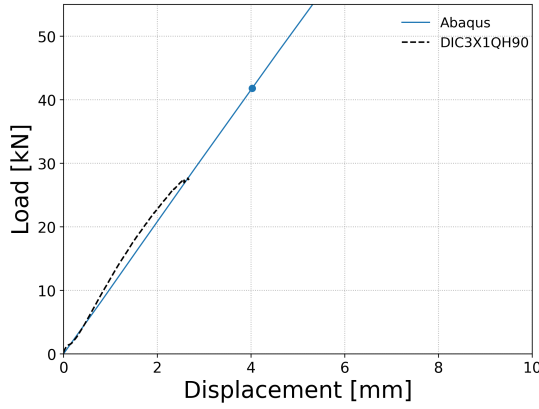
(b) DIC 3x1 DDQI elastic.



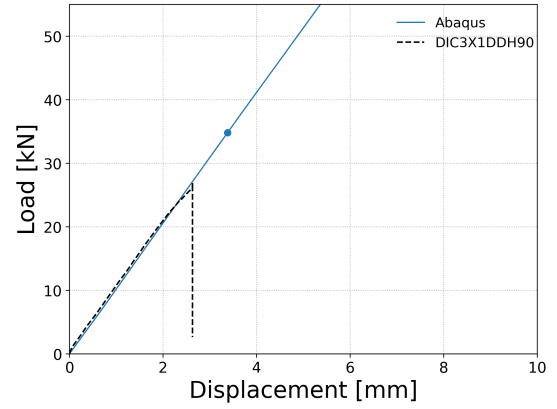
(c) DIC 3x1 QH elastic.



(d) DIC 3x1 DDH elastic.



(e) DIC 3x1 QH90 elastic.



(f) DIC 3x1 DDH90 elastic.

Figure 4.12: Linear elastic curves extracted for the 3×1 configurations compared with experimental curves, note that some plots are not using DIC load-displacement curve.

4.2.3 Calibration through average stiffness values

To evaluate the influence of the calibration parameters, additional simulations were performed using the average stiffness values from Table 4.2 for both the bearing and pull-through responses, as detailed in Table 4.6. This methodology was adopted based on the prediction that the pull-through and bearing stiffness values would remain uniform across all configurations, namely QH, QH90, DDH, and DDH90.

Table 4.6: Average stiffness values for Pull-Through and Bearing.

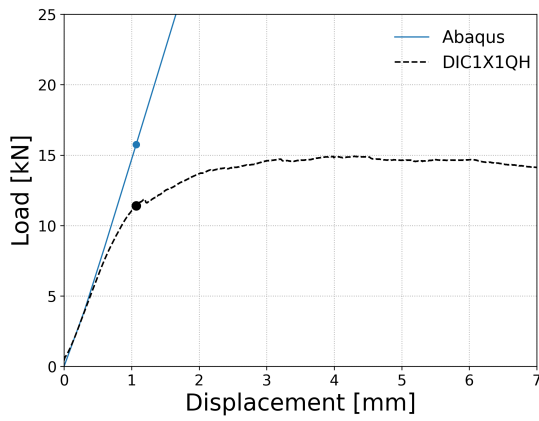
PT (N/mm)	Bearing (N/mm)
26500	74000

Comparing the current results in Table 4.7 with the values previously presented in Table 4.3, it can be confirmed that they are quite similar. This indicates that at higher stiffness levels, a significantly larger increment is required to produce a noticeable change. Consequently, the calibration process for Hard layup sequences can be performed using only two high-fidelity tests: one for bearing and one for pull-through. The same approach can be applied to Quasi-isotropic layup sequences.

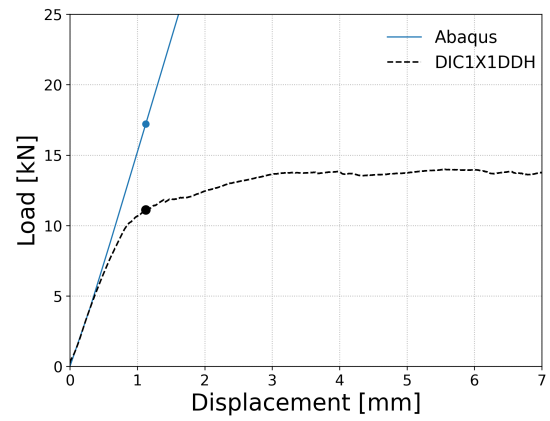
Table 4.7: First non-linearity on Abaqus curve compared to the same displacement on the experimental curve.

Case	Abaqus Load (kN)	Experimental Load (kN)	Displacement (mm)	Error (%)
QH	15.760	11.510	1.060	36.9
DDH	17.350	11.240	1.100	54.4
DDH90	12.120	10.770	1.350	12.5
QH90	14.700	10.230	1.620	43.7

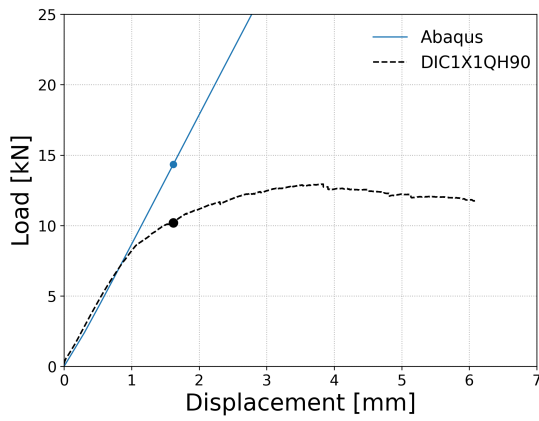
As illustrated in the load-displacement curves in Figure 4.13, the elastic region exhibits a high degree of similarity to the experimental data, mirroring the results obtained with the previous calibration values.



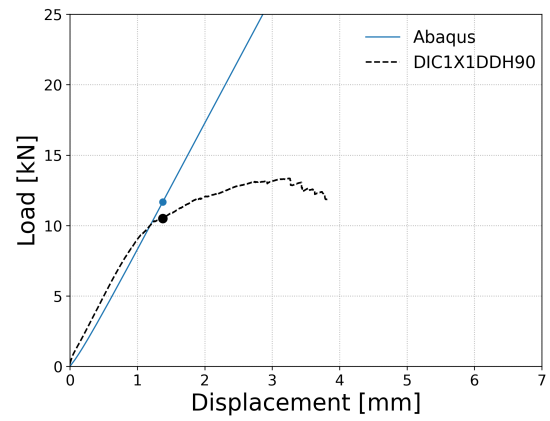
(a) DIC 1x1 QH modified elastic region.



(b) DIC 1x1 DDH modified elastic region.



(c) DIC 1x1 QH90 modified elastic region.



(d) DIC 1x1 DDH90 modified elastic region.

Figure 4.13: Modified linear elastic response curves extracted for the 1×1 configurations.

4.3 Method 2-Plastic calibration

This method involves the introduction of a plasticity model into the Cartesian connector, while retaining the calibrated elastic properties established in the first method. The plasticity parameters are calibrated based on the experimental bearing curves; specifically, for each layup, sequential points are extracted from the post-yield regime starting from the initiation of the first non-linearity up to the ultimate peak load. Subsequently, these coordinates, along with the relative plastic displacement between each consecutive point, are implemented into the plasticity section of the connector definition. So this process is applied to each configuration where through Abaqus is made a Force-Displacement curve that can be seen in the Figures 4.15. Similar to the first method, the force-displacement curves were extracted from the connector responses to determine their intersection with the failure envelope, with the corresponding results disclosed in Table 4.8. A limitation of this method was the unavailability of an experimental displacement curve from the bearing DIC test. Consequently, it was not possible to calibrate the simulated bearing response against DIC experimental data; thus, the bearing values were implemented without modification as shown on Figure 4.14. Furthermore, using the plasticity data derived from the bearing test introduces a significant discrepancy in the single lap shear simulation, leading to a maximum load that deviates considerably from the experimental results. Specifically, calibrating with the bearing test introduces an unrealistic plastic zone. While the experimental single lap shear curves exhibit extensive plastic deformation, the plastic region derived from the bearing curve is substantially shorter. This mismatch introduces a secondary error into the approach. These discrepancies are illustrated in Figures 4.15 to 4.17, which compare the experimental and simulated curves.

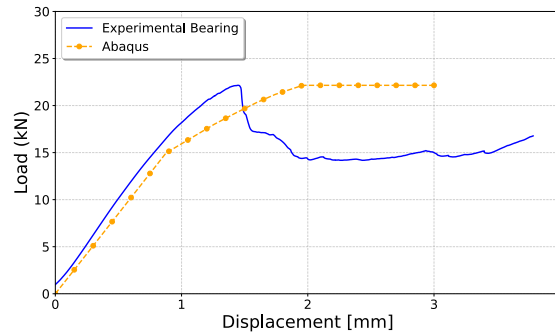


Figure 4.14: Comparison of the load-displacement curves for the bearing behavior between the experimental data (Experimental Bearing) and the calibrated numerical model (Abaqus).

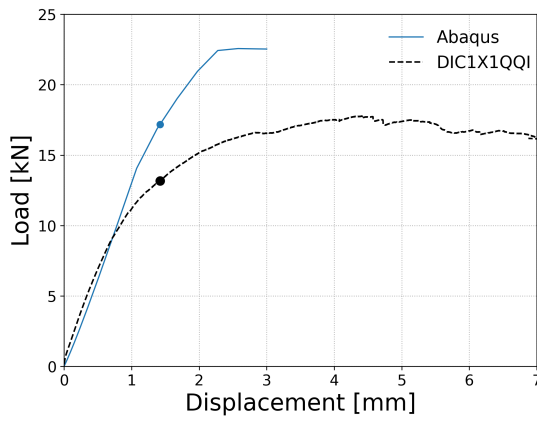
Table 4.8: Plasticity values taken from bearing curves for each layup sequence.

Layup Sequence	Displacement (mm)	Load (kN)	Relative Displacement (mm)
QQI	1.000	15.000	0.000
	1.100	16.500	0.100
	1.200	18.000	0.200
	1.300	19.200	0.300
	1.400	20.300	0.400
	1.500	21.200	0.500
	1.660	22.150	0.660
DDQI	1.000	15.000	0.000
	1.100	16.400	0.100
	1.200	17.200	0.200
	1.300	18.570	0.300
	1.400	19.480	0.400
	1.500	19.790	0.500
	1.580	20.140	0.580
QH	0.790	11.160	0.000
	0.900	12.960	0.110
	1.000	14.440	0.210
	1.100	15.650	0.310
	1.200	16.380	0.410
	1.300	17.100	0.510
	1.400	17.590	0.610
	1.510	17.720	0.720
DDH	0.730	11.477	0.000
	0.800	12.390	0.070
	0.900	13.830	0.170
	1.000	15.040	0.270
	1.100	15.850	0.370
	1.200	16.880	0.470
	1.330	17.450	0.600
DDH90	1.000	11.450	0.000
	1.100	12.790	0.100
	1.200	13.776	0.200
	1.300	14.410	0.300
	1.400	14.770	0.400
	1.500	15.340	0.500
	1.600	15.690	0.600
	1.660	16.110	0.660
QH90	1.000	11.000	0.000
	1.100	12.000	0.100
	1.200	13.000	0.200
	1.300	13.600	0.300
	1.440	14.240	0.440

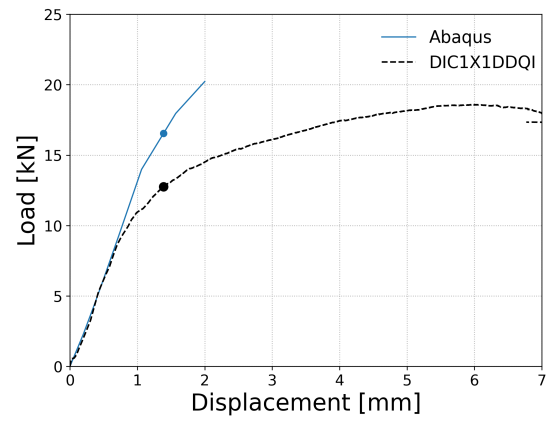
4.3.1 Results

Following the same procedure outlined in Method 1, the non-linearity threshold was determined by the intersection of the connector load paths with the failure envelope, as summarized in Table 4.9. The resulting plots indicate that this intersection significantly overestimates the onset of the first non-linearity, as the predicted point already lies deep within the plastic zone. This primary discrepancy occurs because specimens subjected to single-lap-shear tests exhibit extensive plastic deformation that is challenging to replicate in Abaqus without utilizing an experimental single-lap-shear test for the calibration process.

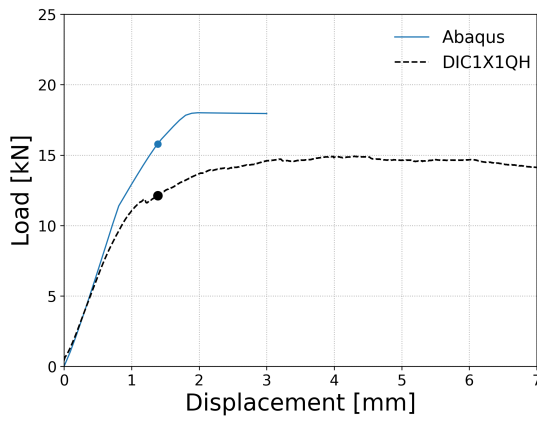
Consequently, relying on plasticity properties calibrated exclusively via standalone bearing tests introduces inherent errors. It not only artificially delays the predicted onset of non-linearity, giving predictions within the plastic range fundamentally flawed, but also introduces a significant discrepancy in the ultimate single-lap-shear response. Specifically, this approach leads to a maximum load that deviates considerably from the experimental results by introducing an unrepresentative plastic zone; while the experimental single-lap-shear curves exhibit extensive, stable plastic deformation, the plastic region derived from the pure bearing curve is substantially shorter. This kinematic mismatch introduces a secondary error into the framework, as illustrated in Figures 4.15 to 4.17, which compare the experimental and simulated curves.



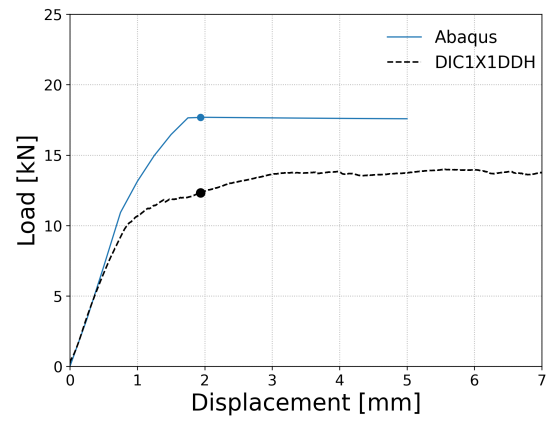
(a) DIC 1x1 QQI plastic.



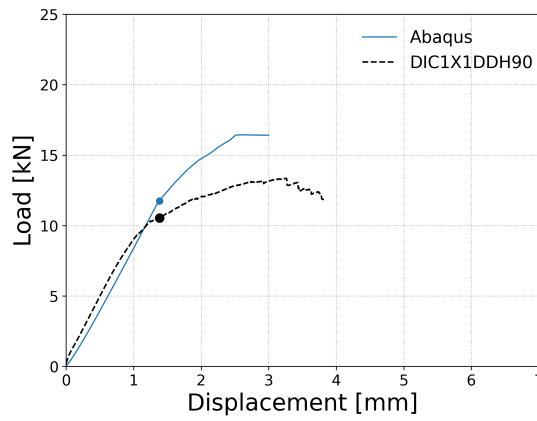
(b) DIC 1x1 DDQI plastic.



(c) DIC 1x1 QH plastic.

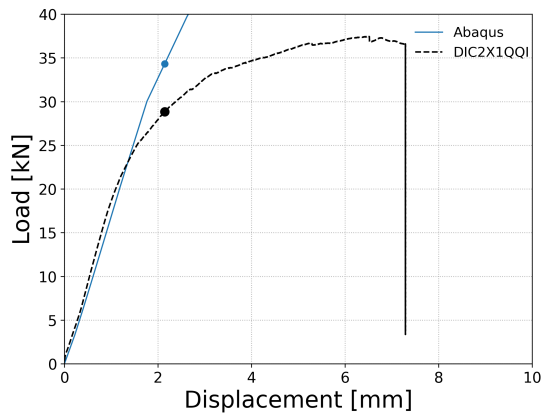


(d) DIC 1x1 DDH plastic.

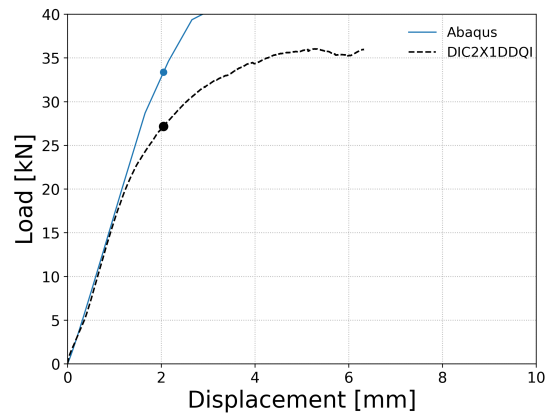


(e) DIC 1x1 DDH90 plastic.

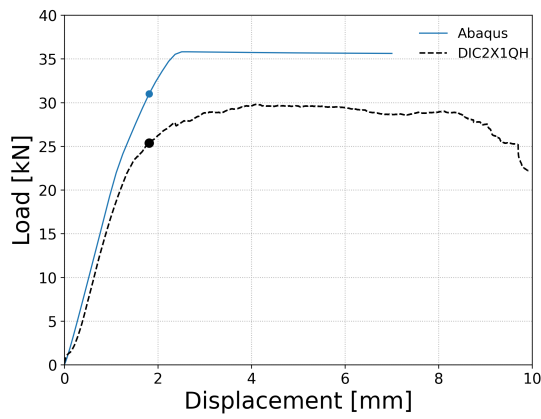
Figure 4.15: Multi-linear connector plasticity curves extracted for the 1×1 configurations, note that some plots are not using DIC load-displacement curve..



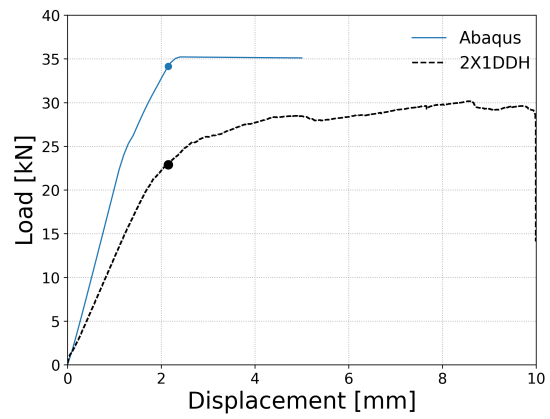
(a) DIC 2x1 QQI plastic.



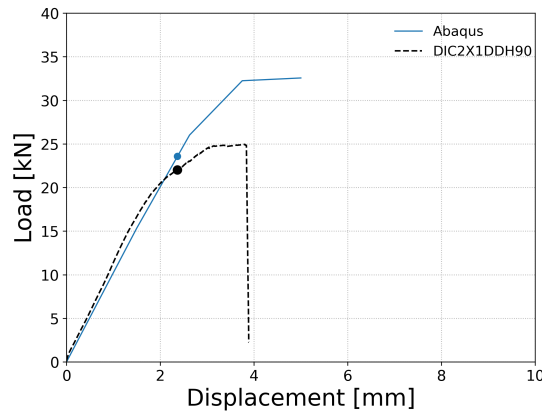
(b) DIC 2x1 DDQI plastic.



(c) DIC 2x1 QH plastic.

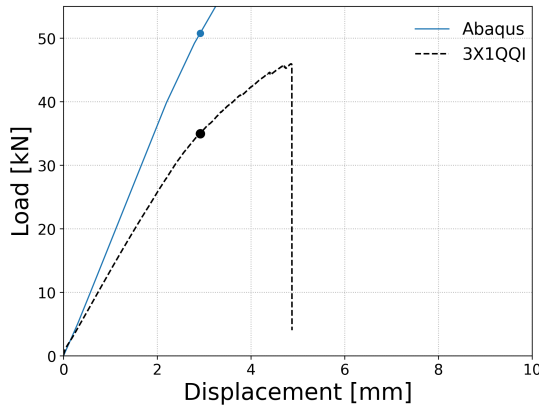


(d) 2x1 DDH plastic.

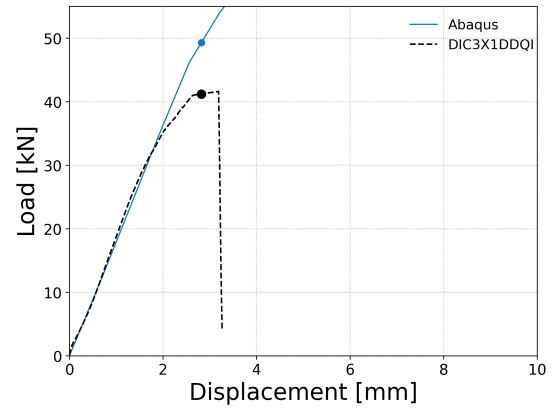


(e) DIC 2x1 DDH90 plastic.

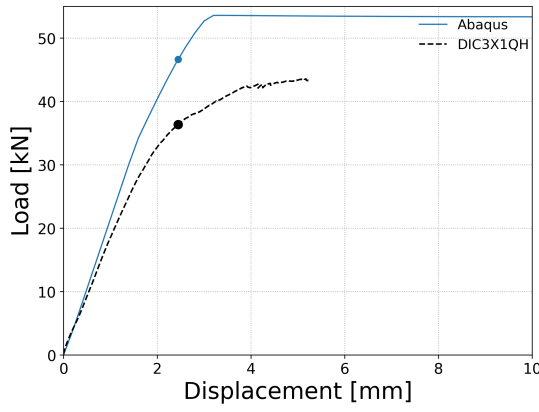
Figure 4.16: Multi-linear connector plasticity curves extracted for the 2×1 configurations, note that some plots are not using DIC load-displacement curve..



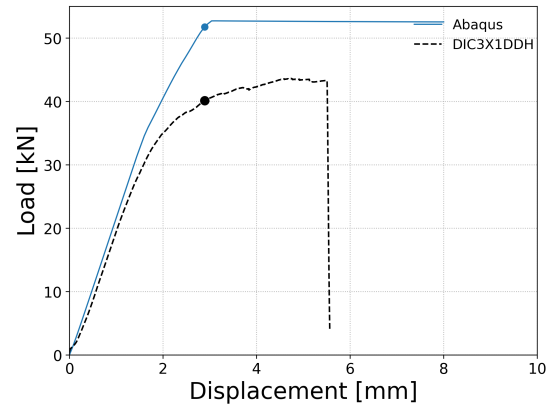
(a) 3x1 QQI plastic.



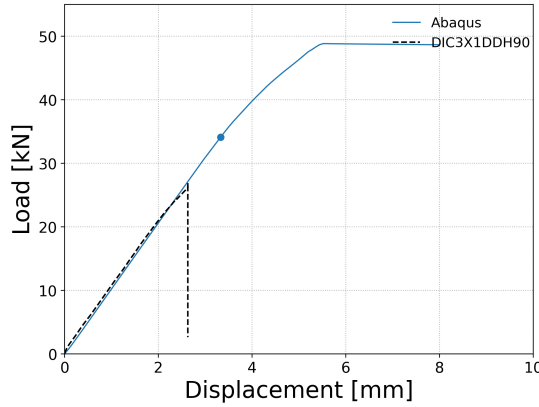
(b) DIC 3x1 DDQI plastic.



(c) DIC 3x1 QH plastic.



(d) DIC 3x1 DDH plastic.



(e) DIC 3x1 DDH90 plastic.

Figure 4.17: Multi-linear connector plasticity curves extracted for the 3×1 configurations, note that some plots are not using DIC load-displacement curve.

In Table 4.9, the QH90 layup sequence is omitted because this approach failed to yield an intersection with the failure envelope. This occurs because the x-direction (corresponding to CTF1) undergoes plastic deformation, and the methodology imposes a maximum load limit on it. Meanwhile, since the z-direction in this method was calibrated strictly for

elasticity, CTF3 eventually begins to decrease, as illustrated in Figure 4.18. Ultimately, the combined response of CTF1 and CTF3 does not intersect the failure envelope.

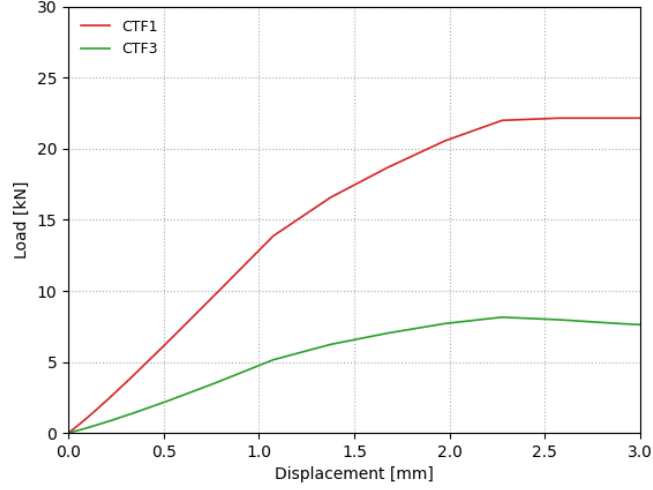


Figure 4.18: 1x1QH90 CTF1 and CTF3 Load-Displacement curve.

Table 4.9: First non-linearity points for different configurations: Numerical vs. Experimental.

Layup	1x1 Num (kN)	1x1 Exp (kN)	2x1 Num (kN)	2x1 Exp (kN)	3x1 Num (kN)	3x1 Exp (kN)
QQI	17.100	13.410	34.300	28.823	51.600	35.492
DDQI	16.500	12.662	33.300	29.948	50.000	41.570
QH	15.790	12.120	31.700	25.240	46.800	36.637
DDH	17.670	12.533	34.300	22.961	51.400	40.206
DDH90	11.730	10.772	23.800	22.500	34.500	—

4.4 Method 3-Calibration using a experimental single lap shear

An alternative methodology, designated as Method 3, was developed to calibrate both the elastic and plastic connector properties directly from the experimental force-displacement curves obtained from the QOI 1x1 single-lap-shear test. As previously established in Chapter 3, this approach strictly required the deployment of experimental curves derived from Digital Image Correlation (DIC) measurements to ensure spatial accuracy. In the first stage of this protocol, the linear elastic response was calibrated by matching the numerical slope with the experimental DIC curve within the initial elastic regime of the QOI 1x1 specimen. During this phase, an isotropic stiffness assumption was enforced, meaning that all three translational coordinates of the Cartesian connector elements were assigned an identical stiffness value.

In the second stage, mirroring the sequential point-extraction method implemented in Method 2, discrete coordinates were extracted from the post-yield regime of the experimental curve. This extraction window spanned from the onset of deviation from linearity up to the first notable load drop in the force-displacement history. Subsequently, iterative numerical simulations were conducted to refine these plastic data points until the simulated post-yield profile closely replicated the experimental DIC baseline. The final calibrated connector properties are summarized in Tables 4.10 and 4.11, while the resulting correlated curves are illustrated in Figures 4.19 and 4.20.

Table 4.10: Connector stiffness property.

Parameter	Value (N/mm)
Connector stiffness	85000

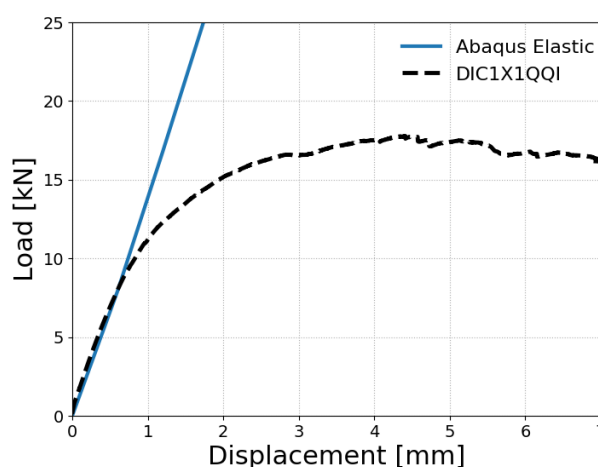


Figure 4.19: Elastic calibration through 1x1 QOI.

Table 4.11: Plasticity calibration data for the numerical model.

Force (N)	Relative Displacement (mm)	Abaqus Calibration
9683	0.000	0.000
11106	0.200	0.100
13000	0.700	0.400
14787	1.200	0.900
16027	1.700	1.400
16648	2.200	1.900
17237	2.700	2.400
17812	3.200	2.900
18070	3.700	3.400
18313	4.100	3.800

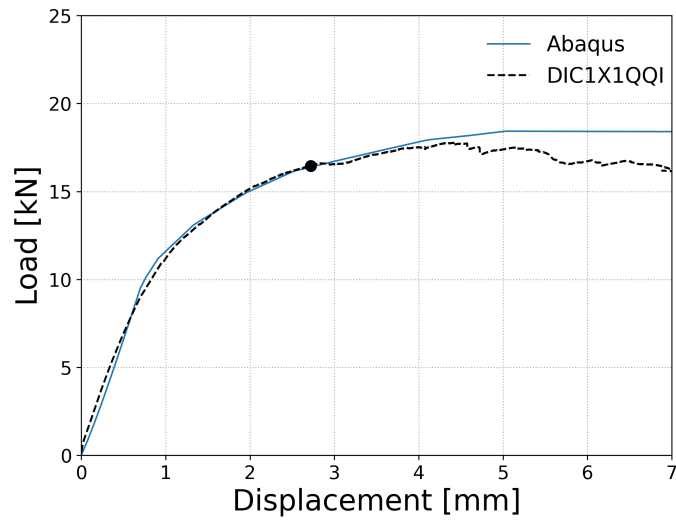


Figure 4.20: Calibration through QQI Load-Displacement curve.

4.4.1 Results

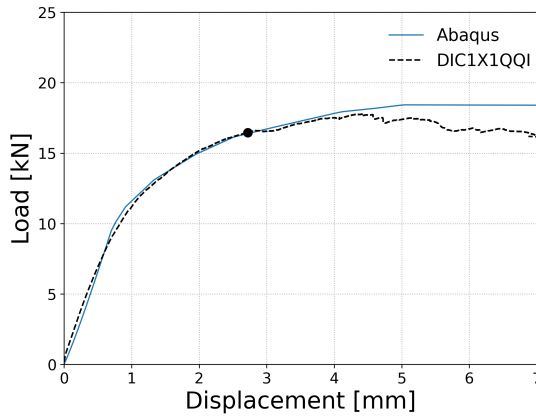
Similarly to the procedures implemented in Methods 1 and 2, the first non-linearity was determined through the intersection of the failure interaction envelopes with the Cartesian connector load paths, with the corresponding numerical results presented in Table 4.12. However, this methodology introduces specific limitations that must be addressed. First, it requires an additional experimental single-lap-shear test configuration to serve as a baseline. Furthermore, applying the identical post-yield plasticity curve derived from the QQI 1x1 configuration across all remaining laminate sequences introduces a geometric and constitutive simplification error; as demonstrated in Chapter 3, the post-yield behavior within the plastic zone varies significantly among different laminate architectures. This discrepancy is particularly amplified in the non-quasi-isotropic (hard) laminates, as their nominal thickness is lower than that of the quasi-isotropic sequences, inherently inducing secondary errors in the hard layup predictions.

Nevertheless, this approach proves valuable as it enables a reasonable structural estimation for multiple laminate configurations based on a single experimental baseline. In comparison with the alternative numerical methods explored, Method 3 successfully circumvents the necessity for high-fidelity 3D finite element simulations, shifting the calibration dependency strictly onto a single, localized experimental test setup the 1x1 QQI single-lap-shear configuration.

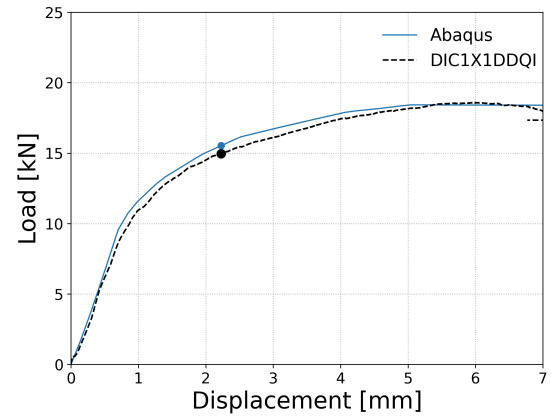
Table 4.12: First non-linearity values for each configuration: Numerical vs. Experimental and relative error.

Laminate	1x1 Num (kN)	1x1 Exp (kN)	1x1 Err (%)	2x1 Num (kN)	2x1 Exp (kN)	2x1 Err (%)	3x1 Num (kN)	3x1 Exp (kN)	3x1 Err (%)
QQI	16.385	16.385	0.00	29.500	31.300	-5.75	25.231	38.736	-34.86
DDQI	15.526	14.951	3.85	29.046	31.800	-8.66	43.451	41.490	4.73
QH	14.372	12.870	11.67	26.149	24.123	8.40	39.441	34.434	14.54
DDH	15.848	12.872	23.12	28.260	22.642	24.81	42.894	38.512	11.38
QH90	13.269	11.040	20.19	24.277	—	—	36.163	—	—
DDH90	11.160	10.430	7.00	21.048	20.840	1.00	30.333	—	—

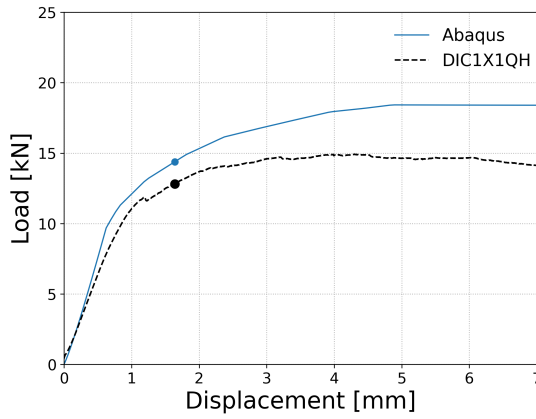
Utilizing the QQI 1x1 single-lap shear test to calibrate the QQI 2x1 configuration yields highly accurate results, as demonstrated in Figure 4.22a. However, for the QQI 3x1 configuration, the absence of DIC test data precludes a comprehensive comparison between the experimental and numerical curves. When applied to the DDQI laminate, this calibration method also provides highly accurate predictions across all configurations, as shown in Figures 4.21b, 4.22b, and 4.23b, further substantiating the structural similarity between the QQI and DDQI layups. In contrast, for the remaining cases, the thickness discrepancy between the layups inherently introduces initial errors. Furthermore, the linear decline and plastic behavior vary among the different layups, whereas this methodology assumes them to be uniform across all cases. Nevertheless, compared to the plasticity approach used in Method 2, this method yields lower values, resulting in a numerical curve that aligns more closely with the experimental data, as illustrated in Figures 4.21 to 4.23. Similar to Method 2, the intersection overestimates the onset of non-linearity, projecting the predicted point into the plastic zone. In this instance, the discrepancy is primarily driven by the failure envelope criteria. As evidenced by Figure 4.20, where the Abaqus curve closely replicates the experimental behavior, the highlighted point indicates precisely where this intersection occurs. As detailed in Chapter 3, the failure envelope was constructed using the ultimate peak load from the bearing test. Utilizing the yield point, the end of the linear elastic zone, instead could potentially provide more accurate predictions.



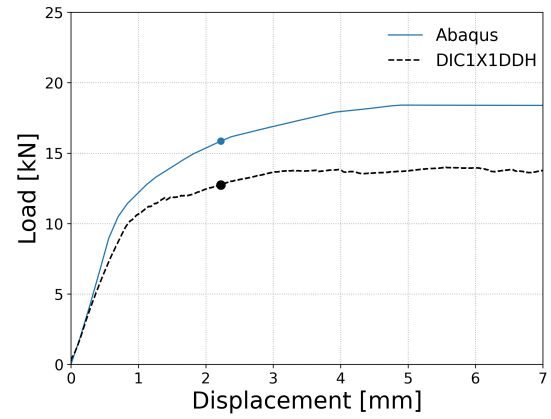
(a) DIC 1x1 QQI configuration.



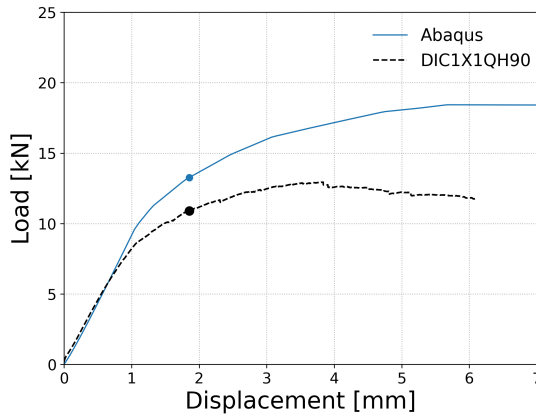
(b) DIC 1x1 DDQI configuration.



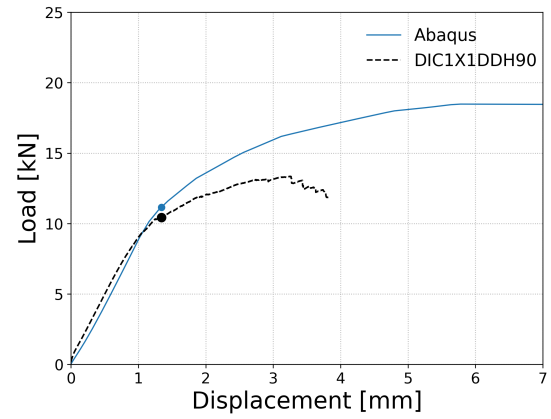
(c) DIC 1x1 QH configuration.



(d) DIC 1x1 DDH configuration.

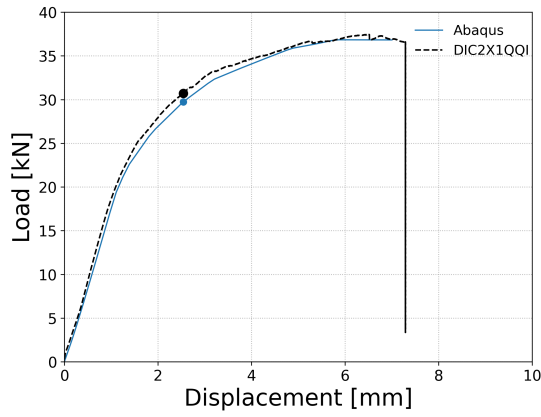


(e) DIC 1x1 QH90 configuration.

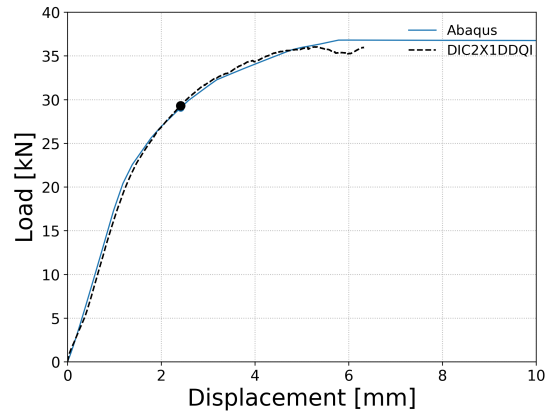


(f) DIC 1x1 DDH90 configuration.

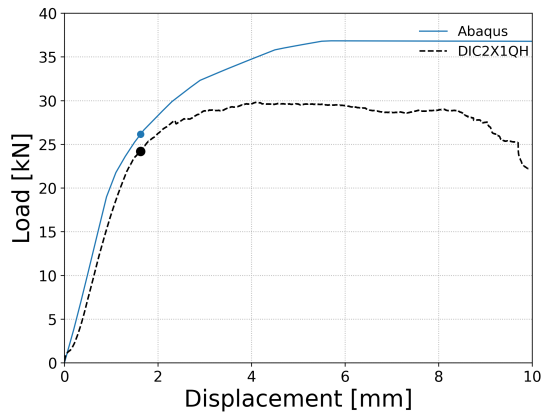
Figure 4.21: Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the 1×1 single-bolt joint configurations across different laminate configurations with the exception of 2×1 DDH and 3×1 QQI.



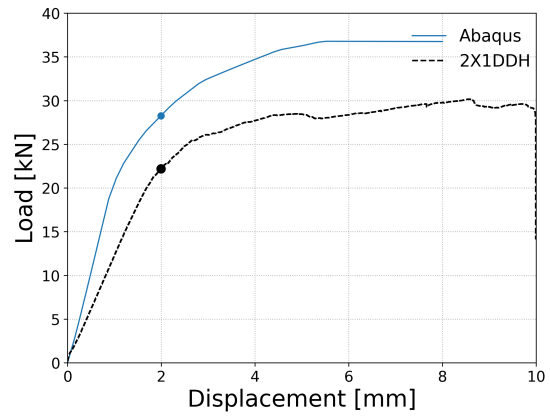
(a) DIC 2x1 QQI configuration.



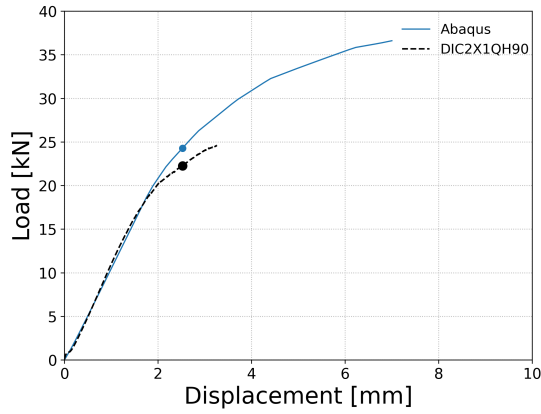
(b) DIC 2x1 DDQI configuration.



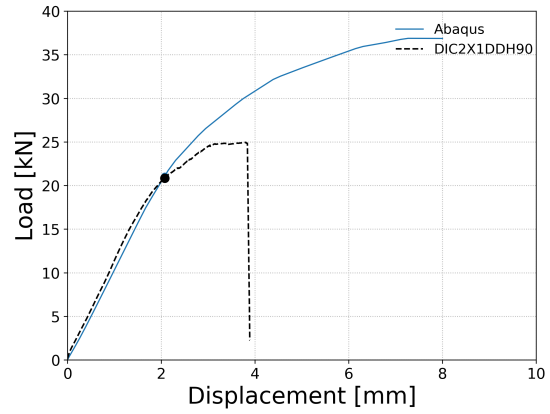
(c) DIC 2x1 QH configuration.



(d) 2x1 DDH configuration.

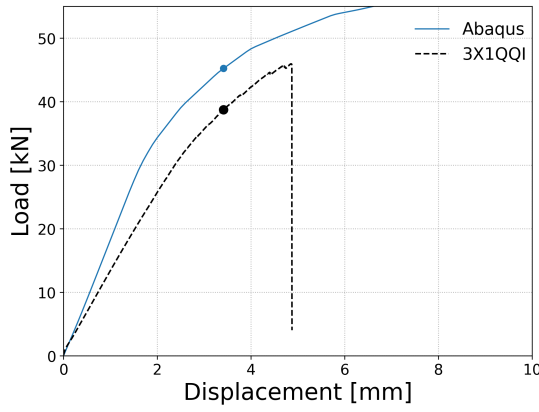


(e) DIC 2x1 QH90 configuration.

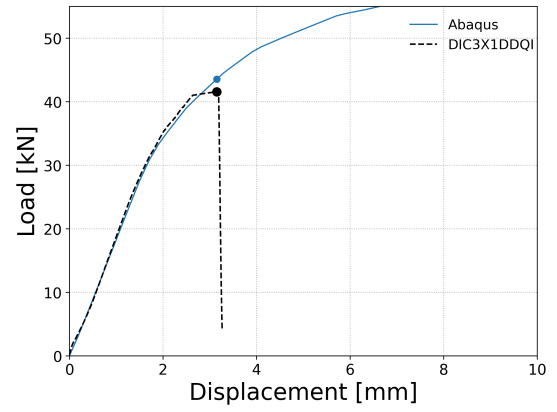


(f) DIC 2x1 DDH90 configuration.

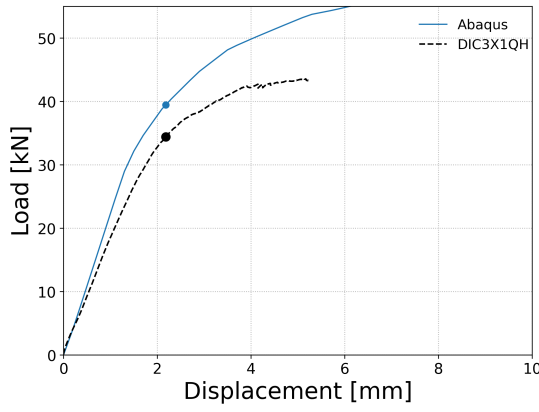
Figure 4.22: Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the multi-bolt (2×1 array) joint configurations across different laminate configurations with the exception of 2x1 DDH and 3x1 QQI.



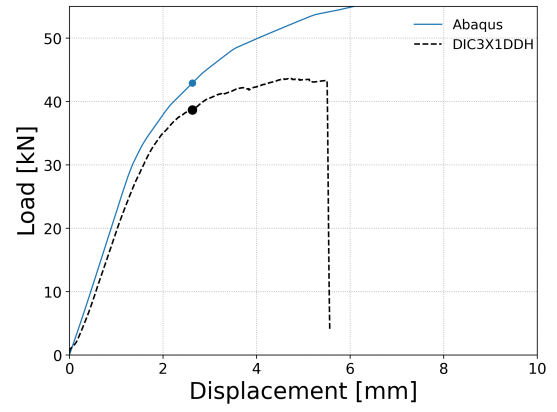
(a) 3x1 QQI configuration.



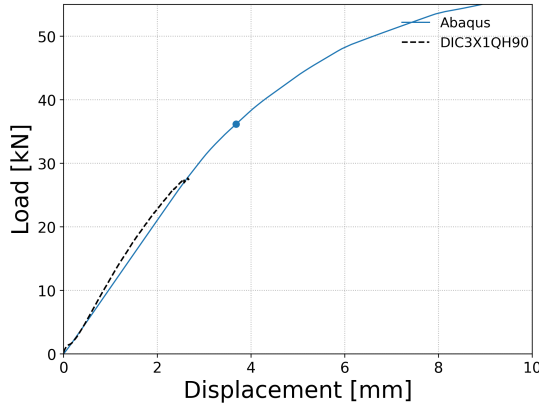
(b) DIC 3x1 DDQI configuration.



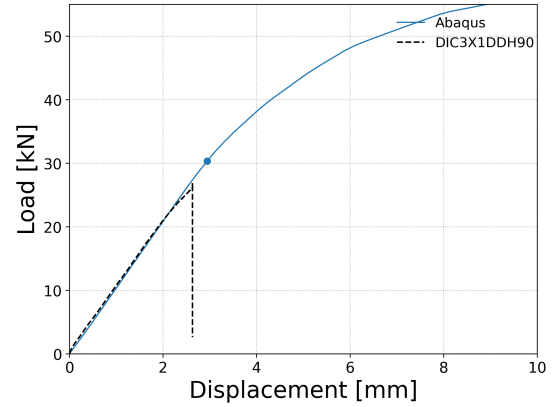
(c) DIC 3x1 QH configuration.



(d) DIC 3x1 DDH configuration.



(e) DIC 3x1 QH90 configuration.



(f) DIC 3x1 DDH90 configuration.

Figure 4.23: Numerical calibration and validation curves comparing Abaqus connector formulations against experimental DIC results for the multi-bolt (3×1 array) joint configurations across different laminate configurations with the exception of 2x1 DDH and 3x1 QQI.

4.5 Bearing/pull-through ratio (n)

To obtain the bearing/pull-through ratio from the Abaqus simulation, Equation 4.1 is utilized:

$$n = \frac{CTF_1}{CTF_1 + CTF_3} \quad (4.1)$$

As observed in Figures 4.24 to 4.26, the elastic model (Method 1) exhibits two distinct stages. The initial adjustment phase at the onset of displacement creates a false impression that the tests begin with pure pull-through, this is merely an artifact of the larger step increments used in Abaqus. This phase is followed by a second stage between $n = 0.6$ and $n = 0.8$, characterized by a negative slope that is consistently followed by all layup sequences. While Methods 2 and 3 display a similar initial trend, their slopes shift upon exiting the elastic regime. This deviation indicates that the influence of bearing failure increases significantly when plasticity is taken into account.

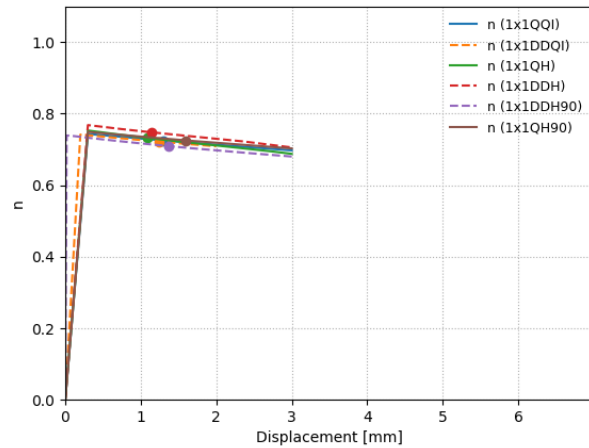


Figure 4.24: Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 1.

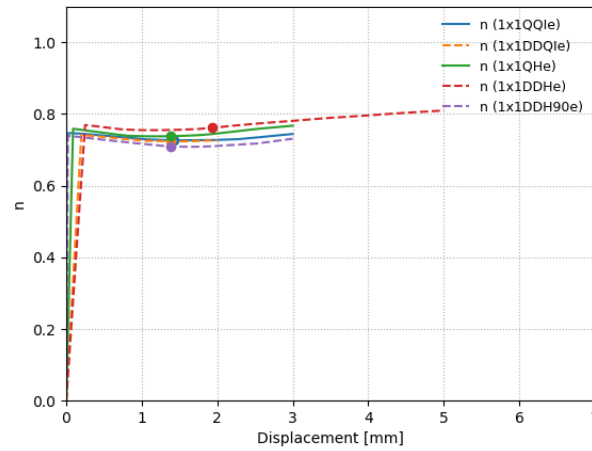


Figure 4.25: Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 2.

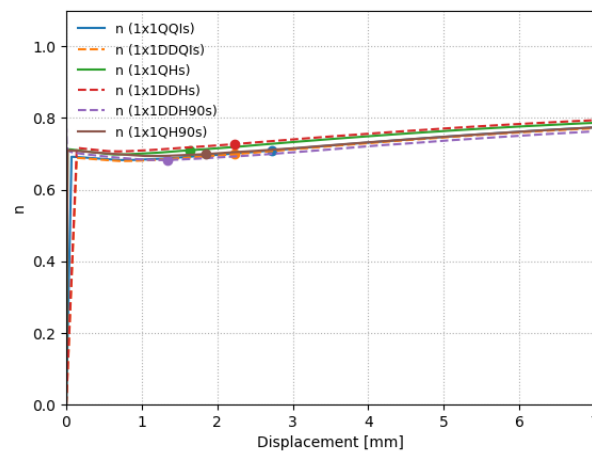


Figure 4.26: Evolution of the normalized ratio n as a function of displacement for the 1x1 configuration: Method 3.

Table 4.13: Exact value of n at the failure envelope intersection.

Laminate	Method 1	Method 2	Method 3
QQI	0.727	0.724	0.709
DDQI	0.724	0.721	0.699
QH	0.738	0.732	0.710
DDH	0.762	0.747	0.727
DDH90	0.709	0.708	0.683
QH90	—	0.724	0.699

Comparing the evolution of n across the different multi-bolt joint configurations, the curves consistently exhibit a plateau or constant zone while the joint remains within the elastic regime. However, upon transitioning into the plastic range, the value of n begins to rise progressively, indicating again a shift toward a more bearing dominated failure influence, as illustrated in Figure 4.30.

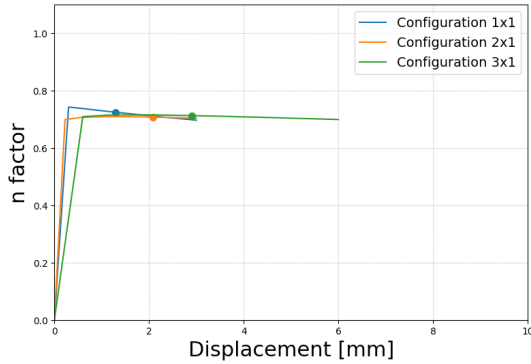


Figure 4.27: Normal method analysis.

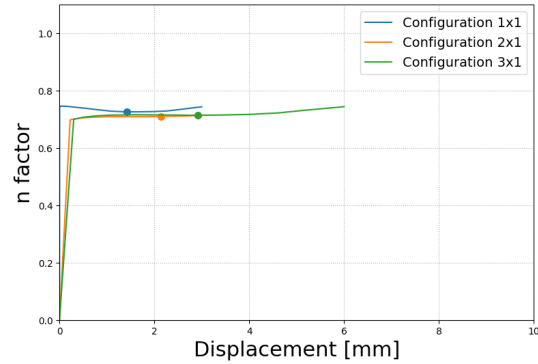


Figure 4.28: Plastic method analysis.

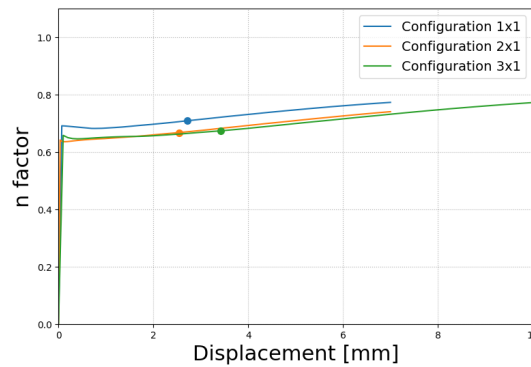


Figure 4.29: SLS method analysis.

Figure 4.30: Comparison of n factor vs displacement for all three methods.

Table 4.14: Comparison of the intersection values of n for Q/QI laminate across different bolt configurations.

Configuration	Method 1	Method 2	Method 3
1x1	0.727	0.724	0.709
2x1	0.708	0.711	0.668
3x1	0.714	0.715	0.674

4.5.1 Concluding Remarks

This section provides a comprehensive summary and critical evaluation of the three connector calibration methods (Method 1: Elastic, Method 2: Pure Bearing Plasticity, and

Method 3: Single-Lap Shear Experimental Calibration). Table 4.15 summarizes the three calibration frameworks.

Table 4.15: Comprehensive synthesis of the advantages, limitations, results, and n -ratio evolution of the three calibration methods.

Methods	Advantages	Limitations	Results	Evolution of Ratio n
Method 1 (Elastic)	• Highly accurate initial elastic stiffness. • Simpler method.	• Only considers elastic behavior. • Requires high-fidelity simulations for calibration.	• Overestimates the load threshold at the failure interaction envelope. • Predicted values lie near the ultimate experimental peak loads.	• Stabilizes at a plateau (0.6 to 0.8) with a slightly negative slope.
Method 2 (Bearing Plastic)	• Accounts for post-yield degradation.	• Resulting load-displacement curve does not match the experimental plastic zone and peak load. • Requires high-fidelity simulations.	• Delays the predicted onset of non-linearity, projecting it deep into the plastic range. • Causes non-intersection and data omission for specific configurations (e.g., QH90).	• Follows Method 1 initially, then exhibits a progressive upward slope, indicating bearing dominance.
Method 3 (SLS Plastic)	• Eliminates the necessity for high-fidelity simulations. • The connector uses the same value on all three coordinates.	• Requires an additional experimental test. • Assumes the identical plasticity parameters uniformly across all layup sequences.	• Achieves high curve replication accuracy for multi-bolt arrays (2×1 and 3×1). • Values closer to experimental baseline curves.	• Displays a similar initial elastic plateau, shifting to a more pronounced increase in n during advanced plastic deformation.

Chapter 5

Conclusion and Future Work

5.1 Conclusion

The project presented in this thesis was driven by two primary objectives aligned with advancing next-generation composite structures. The first objective was to experimentally characterize the mechanical response of novel Double-Double (DD) laminates, directly comparing their structural performance against conventional Quad-axis bolted joints across pure bearing, pull-through, and multi-bolt regimes. The second objective focused on developing and validating an efficient numerical methodology compatible with industrial workflows, using low-fidelity connector-based formulations in Abaqus to predict large-scale joint behavior. To achieve these dual goals, single-bolt and multi-bolt single-lap shear joint configurations were systematically investigated. These configurations successfully validated an empirical failure interaction envelope generated through a combined bearing/pull-through (BEA/PT) approach, while effectively capturing the load distribution trends and structural performance differences between the DD and Quad laminate stacking sequence.

First, defining a robust experimental testing procedure and instrumentation layout for model validation, followed by the development of laminate-level numerical models using shell elements integrated with non-linear bolt connectors and predefined failure criteria. Ultimately, the numerical predictions, specifically regarding the first non-linearity were validated against experimental data.

Single-lap shear joints were investigated to generate a failure envelope combining bearing, pull-through, and simultaneous bearing/pull-through loading conditions. The experimental results demonstrated that the DD configuration exhibits highly similar behavior to the Legacy Quad configuration across both the individual single-lap shear tests and the overall failure envelope. However, the data points utilized to construct this failure envelope may lack the precision required to seamlessly combine with computational simulations for an accurate prediction of the first non-linearity.

Finally, an efficient numerical modeling approach utilizing Cartesian connector elements and planar shell elements was implemented to simulate the single-lap shear, pure bearing, and pure pull-through tests, evaluating three distinct calibration methodologies.

The stiffness calibration executed in Method 1 proved to be highly accurate, yielding numerical linear elastic curves that closely matched the experimental data. Furthermore, the alternative calibration using average stiffness values produced results remarkably similar to those of Method 1, this crucial finding implies that future simulation frameworks can be requiring only two high-fidelity simulations, one for bearing and one for pull-through since the calibrated stiffness values can be assumed uniform between the Quad and DD configurations.

Regarding the prediction of the first non-linearity, Method 2 revealed that calibrating the Cartesian connector plasticity explicitly through experimental bearing tests fails to generate load-displacement curves that match the experimental single lap shear load-displacement curves, this limitation happens because this approach forces the predicted peak load to equal the ultimate bearing strength, which significantly overestimates the experimental maximum load observed in the single-lap shear configurations. Lastly, Method 3 produced a macroscopic load-displacement curve that closely replicated the experimental profile. Although this approach still did not perfectly capture the exact onset threshold of the first non-linearity, it provided the most accurate approximation among all the evaluated methodologies.

5.2 Future Work

To further advance the findings of this research and address its current limitations, several avenues for future work are proposed. First, as discussed in Chapter 3, the data points corresponding to ($n=0.8$) were influenced by premature fastener failure resulting from experimental fixture constraints. Therefore, future studies should focus on redesigning the test setup to overcome these mechanical limitations.

To assess the scalability and repeatability of this methodology, the experimental and numerical procedures should be expanded across a wider design space, incorporating different joint dimensions, multi-bolt configurations, and alternative laminate layup sequences. From a numerical perspective, introducing progressive damage models into the Cartesian connectors could more accurately capture the post-yielding regime and softening behavior observed in the experimental single-lap shear load-displacement curves. Finally, calibrating the plasticity parameters in Method 2 using load-displacement data obtained through Digital Image Correlation (DIC) would, as shown on Method 3, allow a closer approximation between the experimental curve with the simplified model load-displacement curve.

Bibliography

- M. M. Abdalla, S. Setoodeh, and Z. Gürdal. Design of variable stiffness composite panels for maximum fundamental frequency. *Composite Structures*, 81(2):283–292, 2007.
- ASTM International. Astm d7332/d7332m: Standard test method for measuring the fastener pull-through resistance of a fiber-reinforced polymer matrix composite. Technical Report ASTM D7332/D7332M-07, ASTM International, West Conshohocken, PA, 2007.
- ASTM International. Astm d5766/d5766m: Standard test method for open hole tensile strength of polymer matrix composite laminates. Technical report, ASTM International, West Conshohocken, PA, 2018a.
- ASTM International. Astm d5961/d5961m: Standard test method for bearing response of polymer matrix composite laminates. Technical report, ASTM International, West Conshohocken, PA, 2018b.
- ASTM International. Astm d6484/d6484m: Standard test method for open hole compressive strength of polymer matrix composite laminates. Technical report, ASTM International, West Conshohocken, PA, 2020.
- E. J. Barbero. *Introduction to Composite Materials Design*. CRC Press, 2018.
- P. P. Camanho and M. Lambert. A design methodology for mechanically fastened joints in laminated composite materials. Technical report, Composites Science and Technology, 2006.
- P. P. Camanho and F. L. Matthews. Stress analysis and strength prediction of mechanically fastened joints in frp: a review. *Composites Part A: Applied Science and Manufacturing*, 28(6):529–547, 1997.
- C. Chandregowda and H. N. Suresh. Bushing and beam element formulations for multi-fastener joints in aerospace structures. *International Journal of Aerospace Engineering*, 2018:105–118, 2018.
- F. K. Chang and K. Y. Chang. Post-failure analysis of bolted joints in light-weight composite structures. *Journal of Composite Materials*, 21(11):986–1011, 1987. doi: 10.1177/002199838702101101.

- Dassault Systèmes. *Abaqus Analysis User's Guide*. Dassault Systèmes Simulia Corp., Providence, RI, USA, 2024. Section 25.1: Connectors: Overview.
- M. F. S. F. de Moura, J. P. M. Goncalves, J. A. G. Chousal, and R. D. S. G. Campilho. *Comportamento Mecanico de Materiais Compostos*. Publinindustria, 2005.
- A. Eremin and S. S. Ivanov. Comparison of huth, boeing and swift formulations for fastener compliance in composite plates. In *Proceedings of the International Conference on Composite Materials*, 2021.
- C. Furtado et al. Combined bearing/pull-through failure envelope of composite joints: Experimental setup and numerical validation. *Composites Part A: Applied Science and Manufacturing*, 175:107754, 2023.
- R. F. Gibson. *Principles of Composite Material Mechanics*. CRC Press, 2016.
- R. Gomes, P. P. Camanho, and G. Catalanotti. Influence of laminate architecture on bearing and pull-through failure in composite joints. *Composites Part A: Applied Science and Manufacturing*, 167:107128, 2023.
- M. Gunbring. Evaluation of simplified fastener models in finite element analysis. Technical report, Saab Aerospace Technical Reports, 2008.
- Z. Gürdal and R. Olmedo. In-plane response of laminates with spatially varying fibre orientations: Variable stiffness concept. *AIAA Journal*, 31(4):751–758, 1993.
- L. J. Hart-Smith. Mechanically fastened joints for advanced composites—phenomenological considerations and simple analysis. Technical Report MDC-J3995, Douglas Aircraft Company, 1985.
- L. J. Hart-Smith. Design and analysis of bolted and riveted joints in composite structures. Technical Report NASA-CR-179963, NASA Douglas Aircraft Company, 1986.
- L. J. Hart-Smith. Practical considerations in the design and manufacture of carbon-epoxy aircraft structures. Technical Report MDC-P0152, Douglas Aircraft Company, 1987.
- A. Hawkins, S. L. J. Millen, and A. Aravand. Double-double laminates: a comprehensive assessment of the mechanical performance in comparison to traditional quad laminates. Technical report, Queen's University Belfast Research Portal, 2026.
- H. Huth. Influence of fastener flexibility on the load distribution in bolted joints. *Aircraft Structures*, 12:201–210, 1984.
- M. Ibrahim, M. Abdalla, and Z. Gürdal. Equivalent stiffness design of composite laminates with reduced ply orientations. *Composite Structures*, 108:1–8, 2014.
- T. Ireman. Three-dimensional stress analysis of bolted joints in composite laminates.

- Computational Materials Science*, 20(2):253–266, 2001. doi: 10.1016/S0927-0256(00)00182-3.
- Join3D. "Join3D progress report", internal report, 2026.
- J. Kim, J. C. Yoon, and B. S. Kang. Finite element analysis and modeling of structure with bolted joints. *Applied Mathematical Modelling*, 31(5):895–911, 2007.
- R. Y. Kim and I. M. Daniel. Fatigue behaviour of composite laminates with a center hole. *Journal of Composite Materials*, 31(8):809–833, 1997.
- J. Korolija and M. Marjanovic. Accuracy assessment of 1d connector elements in multi-bolt metallic and composite configurations. *Thin-Walled Structures*, 51:88–97, 2012.
- V. Kradinov, E. Madenci, and D. R. Ambur. Application of a variational method for bolted joints in composite laminates. *Composite Structures*, 79(3):395–404, 2007.
- F. Liu, J. Zhang, and L. Zhao. An improved 2d finite element model for load distribution prediction in multi-bolt single-lap composite joints. *Composite Structures*, 240:112046, 2020.
- C. S. Lopes, P. P. Camanho, Z. Gürdal, and B. F. Tatting. Tailoring for strength and stiffness of composite laminates using tow-steered fibres. *Composites Part A: Applied Science and Manufacturing*, 40(6–7):785–794, 2009.
- F. L. Matthews and P. F. Kilty. Strength of bolted joints in glass fibre reinforced plastics. *Composites*, 14(3):226–232, 1983.
- C. T. McCarthy and M. A. McCarthy. Three-dimensional finite element analysis of multi-bolt composite joints. *Composite Structures*, 62(2):159–175, 2003.
- M. A. McCarthy, C. T. McCarthy, V. P. Lawlor, and W. F. Stanley. Three-dimensional finite element analysis of single-bolt, single-lap composite joints with variable bolt clearance. *Composite Structures*, 57(1-4):401–409, 2002. doi: 10.1016/S0263-8223(02)00108-7.
- M. A. McCarthy, C. T. McCarthy, and G. S. Padhi. A simple method for determining the effects of clearance on load distribution in multi-bolt joints. *Composite Structures*, 68(2):140–158, 2005a.
- M. A. McCarthy, C. T. McCarthy, V. P. Lawlor, and W. F. Stanley. Three-dimensional finite element analysis of single-bolt, single-lap composite bolted joints: part i—model development and validation. *Composite Structures*, 71(2):140–158, 2005b.
- J. D. D. Melo, J. Bi, and S. W. Tsai. Double-double: A new family of composite laminates. *Composite Structures*, 100:237–243, 2017.

- M. C. Y. Niu. *Composite Airframe Structures: Practical Design Information and Data*. Conmilit Press, Hong Kong, 1992.
- Tomás Ribeiro Santos. High- and low-fidelity damage models for composite joints. Master’s dissertation, Faculty of Engineering, University of Porto, Porto, Portugal, 2025.
- S. Shrivastava, N. Sharma, S. W. Tsai, and P. M. Mohite. D and dd-drop layup optimization of aircraft wing panels under multi-load case design environment. *Composite Structures*, 248:112518, 2020.
- C. Soutis. Fibre reinforced composites in aircraft construction. *Progress in Aerospace Sciences*, 41(2):143–151, 2005.
- S. D. Thoppul, J. Finegan, and R. F. Gibson. Mechanics of mechanically fastened joints in polymer–matrix composite structures – a review. *Composites Science and Technology*, 69(3–4):301–329, 2009.
- S. W. Tsai. *Composite Design 2013*. Stanford University Structures and Composites Laboratory, Stanford, CA, 2013.
- S. W. Tsai. Double-double: New family of composite laminates. *AIAA Journal*, 59(11):4305–4035, 2021.
- S. W. Tsai. Double-double: A new family of composite laminates. *AIAA Journal*, 60(8):4415–4423, 2022.
- S. W. Tsai and J. D. D. Melo. *Composite Materials Design and Testing*. Stanford University, 2014.
- S. W. Tsai and J. D. D. Melo. *Composite Materials Design and Testing*. Stanford University Composites Design Group, 2015.
- S. W. Tsai and E. M. Wu. A general theory of strength for anisotropic materials. *Journal of Composite Materials*, 5(1):58–80, 1971.
- S. W. Tsai et al. Laminate-thickness tapering opportunities of double-double (dd) laminates compared to conventional quad laminates. In *Composite Structures / AIAA Conference Proceedings*, 2024.
- U.S. Department of Defense. Mil-hdbk-17-3f: Composite materials handbook - volume 3: Polymer matrix composites materials usage, design, and analysis. Technical report, U.S. Department of Defense, 2002.
- B. Vermes, S. W. Tsai, A. Riccio, et al. Application of the tsai’s modulus and double-double concepts to the definition of a new affordable design approach for composite laminates. *Composite Structures*, 251:113246, 2020.

- C. Verwaerde and P. Alart. A reduced-order model for plasticity and friction interactions in aerospace connectors. *International Journal for Numerical Methods in Engineering*, 122(8):1945–1967, 2021.
- C. Verwaerde, P. Alart, and S. Pagano. Non-linear finite element connector models incorporating friction for multi-bolted joints. *Computational Mechanics*, 66:415–432, 2020.
- A. G. Volpi. Laminate-level design criteria for composite structural joints. Master’s dissertation, Faculty of Engineering, University of Porto, 2024.
- A. G. Volpi et al. Integrating laminate-level bolted joint failure envelope data into connector-based finite element models for composite joint stiffness and failure prediction. *Journal of Composite Materials*, 2026.