

Imperial College of Science, Technology and Medicine  
Department of Aeronautics

**The experimental study and predictive virtual  
testing of biaxial Non-Crimp Fabric (NCF)  
Composites**

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Submitted in part fulfilment of the requirements for the degree of  
Doctor of Philosophy in Aeronautics of Imperial College, April 2022





‘Research is to see what everybody else has seen, and to think what nobody else has thought.’

*Albert Szent-Györgyi*



## Dedication

*To the memory of my beloved grandmother Sophia, who passed away only few weeks before the completion of these studies.*



## Abstract

For many years, NCF composites are under development due to their superior mechanical properties in particular areas when compared against their unidirectional pre-preg taped counterparts. They offer advanced interlaminar and impact properties, reduced manufacturing cost, high level of drapeability but more importantly low production cost with non-autoclave methods (RTM, VI). These advantages have resulted in a grown industrial interest for NCF composites and nowadays in an extensive application to the aerospace, automotive and wind energy sectors. However, NCF composites do have certain disadvantages; due to their internal structure, they exhibit low longitudinal stiffness and compressive strength.

This research project is part of the framework of the Airbus Project, 'The Wing of Tomorrow' and aims to characterise the internal structure, identify the source of structural uncertainties, and comprehend the mechanical response of biaxial NCF composites under quasi-static and impact conditions. An extensive experimental study with deliberate imperfections on NCF composite coupons is performed and includes complete in-plane and interlaminar testing, investigation of damage propagation and low velocity impact (LVI) tests.

Ultimately, the scope of this work is to incorporate the experimental findings to a set of physically based failure criteria, able to predict the damage onset and propagation under quasi-static and impact conditions of the NCF coupons. The proposed criteria account for structural flaws, namely the induced in- and out-of-plane fibre misalignment. Within this study, a novel approach to obtain the effective longitudinal stiffness of the composite, as a function of the in- and out-of-plane fibre misalignment, is presented. Additionally, a new exponential law is introduced during element failure in fibre tension, aiming to capture the load progress in conjunction with the energy release rate while stable damage evolves. Essential influence appears to have in fibre dominated failure modes as in Compact Tension (CT). The failure criteria are also validated against LVI cases with deliberately misaligned and split NCF blankets for energy levels between 30J to 70J and predicted well the mechanical response and the damage modes.

The numerical models are employed in Abaqus/Explicit and the constitutive failure criteria are introduced as a VUMAT subroutine, while they follow the energy-based damage mechanics approach. During computational analyses the mesh objectivity of the model is also studied for every loading scenario, to ensure that acceptable results can be obtained with coarser meshes.



## **Declaration of originality**

The work presented hereafter is based on research carried out by the author at the Department of Aeronautics of Imperial College London and it is all the author's own work except where otherwise acknowledged. No part of the present work has been submitted elsewhere for another degree or qualification.

Dimitris Gouskos

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## Acknowledgements

Several people supported my effort to complete my PhD studies, to whom I would like to express my sincere acknowledgements. First and foremost I am grateful to Professor Lorenzo Iannucci, an ideal teacher, mentor, and thesis supervisor, offering me always advice and encouragement with a smile. I was thrilled by his prominent knowledge in the numerical modelling of composite materials and honoured by his trust to assign me duties next to my research tasks. Concurrently, I am particularly grateful for giving me the opportunity to follow independent paths during the PhD.

EPSRC and Airbus UK are specifically acknowledged for funding this project and providing me all the necessary resources to successfully complete and present this research. Particularly, I would like to take this opportunity and thank Dr Morten Ostergaard and Dr Andy Ibbotson from Airbus UK for the helpful discussions we had throughout this project.

I would also like to extend my sincere thanks to my Late and Early Stage Review assessors, Professor Paul Robinson and Professor Silvestre Pinho, who provided me invaluable feedback on my analysis and reports. Their insightful comments and suggestions had a significant impact on the final result of my research.

During my PhD years, I had the chance to meet great researchers and I would like to express my appreciation to Dr Alireza Razavi, Dr Francesca Pernice, Dr Tomas Katafiasz and Dr Stefano del Rosso for their assistance to adjust myself as a fresher to a new academic environment but more importantly, for sharing their knowledge and expertise. Also, I would like to offer my sincere thanks to Mr Valentin Buchinger who provided me an essential amount of assistance to establish the new RTM facility.

From the department of Aeronautics, I was fortunate to meet Mr Joseph Meggyesi, Mr Keith Wolstenholme and Mr Franco Giammaria with whom I had an excellent collaboration during the experimental part of my study while they were always generous to share their thoughts and experience when I asked for. I would also like to thank Dr Frank Gommer, Mr Gary Senior and Mr Jon Cole for inducting me to the manufacturing of composite materials.

My life at Imperial College would have been much less exciting without the friendship that developed among a few of the Greeks. I would like to thank the great researchers and friends

Dr. Takis Bekas, Dr. Dimitrios Kempesis, Dr. Nasos Giannenas, Dr. Kyriakos Kantarakias for the special moments in and outside college, the lunch and coffee breaks and the help they provided me whenever I needed it. I could not exclude from thanking my lifelong friends from Athens, Rania Markianidou and George Anastopoulos whose friendship honours me for well over 20 years and for being close to me regardless the distance and for the unique moments we had.

It goes without saying that my family deserves endless gratitude: my parents Spyros and Dina as well as my brother Loukas, have always been by my side, supporting my initiatives and guiding me patiently through the years. I always received the support and encouraging words from my aunties Kaiti and Vasso as well as from my grandmother Sophia. A big thank you goes to my family in the UK, auntie Annick and uncle Anthony for being great listeners and offering me a wonderful companion in our Sundays' gatherings during the last ten years that I am living in the UK.

Lastly, a special acknowledgement to Professor Christopher Provatidis, whose lectures and supervision during my undergraduate final year project at the National Technical University of Athens triggered my enthusiasm in computational mechanics and composite materials.

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# Chapter 1

## Introduction

### 1.1 Background

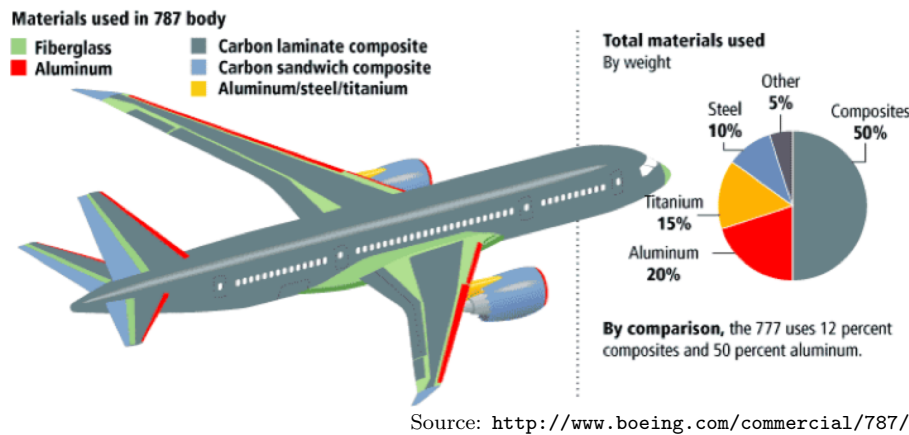
The demand of lightweight structures in the disciplines of aerospace, automotive and defence has been highly increased over the past 40 years. As a result of this the application of composite materials has grown proportionally, particularly by replacing metallic alloys, aluminium and light materials in numerous structures. Since late 1990's, composites are extensively used by the two major aircraft manufacturers (Figs. 1.1, 1.2). Whilst during the early years of their application, composite materials, and specifically pre-impregnated 2D laminates (UDPT), were mainly used in secondary aerospace structures; in the recent years advanced composite materials are implemented in several primary structural applications. The first step was done by Airbus in 1990 when composites were introduced for the manufacturing of the vertical and horizontal stabiliser of the A320 family [1]. The main advantages of composite materials compared to conventional metals are, but not limited to, the superior mechanical properties, high strength to weight ratio and the high level of corrosion resistance. It is significant to mention that weight reduction in secondary structures reaches even 20% compared to aluminium alloy structures while the respective value for primary structures may come up to 10% [2]. At the recent years, the amount of CFRP used in the aerospace industry has been gradually increased. The Airbus A350 XWB reaches 53% by weight, mainly due to the manufacturing of its fuselage and wings

by advanced composites (citation). However, a challenge that still needs to be overcome is their current high manufacturing cost (i.e. UDPT) which prevents their global application in civil aviation.



Source: <http://www.airbus.com/aircraft/passenger-aircraft/a350xb-family.html>

Figure 1.1: Materials breakdown in Airbus A350-XWB.



Source: <http://www.boeing.com/commercial/787/>

Figure 1.2: Materials breakdown in Boeing 787-Dreamliner.

The need to overcome this disadvantage led to the development of other types of composites reinforcement such as woven blankets and non-crimp fabrics (NCF). This study is exclusively focused on cross-ply NCF composites. Among their advantages when compared with other types of composites, NCF are characterised by reduced manufacturing cost [3] and a high level of drapeability [4]. The combination of their low production cost with non-autoclave liquid moulding methods, e.g. Resin Transfer Mould (RTM) and Vacuum Infusion (VI), together with certain advantages in their mechanical response (e.g. good interlaminar properties, improved impact behaviour when compared to UDPT [5]), results in lighter structures with reduced

operational costs and in an increased application of NCF composites to the aerospace [6], automotive [7,8] and wind energy sectors [9]. Nevertheless, challenges appear in terms of their mechanical response under compression and matrix dominated failure modes. The advantages and disadvantages of their mechanical properties are extensively discussed in Chapters 2 and 3 of the present thesis.

## 1.2 Motivation and Objectives

The motivation for this research is originated by the demands of the aerospace industry which were previously stated. The current research project is co-funded by the EPSRC (EP/R511961/1) and Airbus UK, and more specifically, is part of the framework of the Airbus Project, 'The Wing of Tomorrow', whose objective is to predict the failure of an NCF composite wing prior to the mechanical testing, without calibrating the constitutive model.

Many aspects of NCF composites though remain unclear and they are either not fully understood or not accounted in the numerical modelling. For instance, the influence of both the in- and out-of-plane fibre misalignment to the prediction of the longitudinal stiffness and strength reduction, or to the structural integrity of low velocity impact (LVI) panels. Moreover, the comprehension of failure mechanisms for the same material system under different loading scenarios is essential for the engineers to avoid over-dimensioning structural components while meeting all safety requirements.

Therefore, in this project a wide experimental study is performed for cross-ply NCF composites, employing standardised and non-standardised test methods at coupon level. The mechanical testing included in-plane tests (i.e. tension, compression and shear), inter-laminar fracture toughness in Mode I and Mode II, translaminar fracture toughness (i.e. Compact Tension) and LVI loading cases. Hence, a database of mechanical properties is generated to validate the numerical model. Additionally, particular emphasis was given to the examination of the internal architecture as well as to the typical failure mechanisms of cross-ply NCF composites, with the assistance of inspection techniques (i.e. C-Scan, Optical Microscope, Scanning Electron

Microscope).

Concurrently, the numerical modelling strategy followed in this research is based on Virtual Testing [10], a predictive numerical method, which aims to reduce the required number of mechanical tests for material characterisation and computational time within the industrial development cycle. Ultimately, the objective of the present PhD project is to develop a set of failure criteria capable to predict the mechanical behaviour of NCF composite coupons under quasi-static and impact conditions, under the frame of an energy-based damage mechanics constitutive model. The numerical results are compared with experimental test data. The new constitutive model is based on the 2D energy-based failure model created by Iannucci et al. [11–13] for thin UDPT and woven laminates introducing layer level failure criteria.

Hence, the objectives of the current research project are summarised as follows:

- Experimentally study the internal structure at mesoscale level of NCF composites, generate a pool of data and characterise the behaviour of NCF coupons through mechanical testing under quasi-static and impact conditions and identify the failure mechanisms under each loading scenario.
- Identify the effect of deliberate imperfections within the internal structure of NCF composites under specific loading conditions, e.g., compression and LVI.
- Develop a set of physically-based failure criteria able to predict damage onset and propagation in NCF composite coupons in quasi-static and dynamics scenarios.
- Introduce imperfections of NCF mesoscale architecture (e.g. fibre waviness) and manufacturing defects (e.g. blanket misalignment) into the failure criteria.
- Reduce the number of the mechanical tests at coupon level for material characterisation and limit the need of the required inputs for the constitutive model to be directly obtained from experiments.



## 1.3 Thesis Outline

The thesis is organised in to the following chapters:

- **Chapter 2:** In Chapter 2 a literature review is presented providing an overview of the in-plane, interlaminar, translaminar and impact properties of NCF composites, as well as of the developed numerical failure models particularly for this type of materials.
- **Chapter 3:** This chapter outlines the experimental procedures and observations which include the manufacturing methods of the laminates, calculation of the volume fractions of the constituents and investigation of the internal structure of NCF composites. Particular focus is given to the identification of specific imperfections mainly expressed as fibre undulations in- and out-of-plane.
- **Chapter 4:** Additionally, the mechanical testing under various loading conditions is thoroughly described while extensive post-mortem fractography on the failed specimens is performed and the failure mechanisms for each case are identified.
- **Chapter 5:** Based on the experimental observations described in Chapters 3 and 4, the development of the new constitutive failure model for the NCF composites is explained in detail and compared against the analytical formulation of the original version of the energy-based damage model [12].
- **Chapter 6:** In Chapter 6 the constitutive model is validated through one element analyses, while the finite element implementation is also presented. Moreover, the numerical validation of the interlaminar properties is performed using FE methods for the numerical analysis of DCB and 4pt-ENF tests.
- **Chapter 7:** The failure onset is numerically predicted under the principal in-plane loading cases (tension, compression and in-plane shear) and compared against the experimental results presented in Chapter 4, utilising the newly developed constitutive failure model.

- **Chapter 8:** The numerical modelling of damage propagation through the mechanical response of a NCF CT specimen is described in detail. An exponential unloading law is implemented in fibre tension failure and compared against a conventional linear unloading law.
- **Chapter 9:** The mechanical response of NCF plates with deliberate imperfections under low velocity impact are studied both experimentally and numerically. Hence, the prediction capabilities of the constitutive model against specific LVI cases are evaluated.
- **Chapter 10:** The conclusions of the PhD study and the proposals for future work are presented in this chapter.
- **Appendices:** Appendix A includes experimental data extracted from mechanical testing, the actual dimensions of the tested specimens as well as the experimental results calculations are presented; in Appendix B the material properties and parameters of the numerical modelling are included.

## 1.4 Publications

Parts of the work presented in this thesis are published or under submission for publication.

### Journal Publications

- D. Gouskos, L. Iannucci, A failure model for the analysis of cross-ply Non-Crimp Fabric (NCF) composites under in-plane loading: Experimental and numerical study. *Engineering Fracture Mechanics*, Volume 271, August 2022.
- D. Gouskos, L. Iannucci, An experimental and numerical study on the translaminal fracture of cross-ply Non-Crimp Fabric composites. *Engineering Fracture Mechanics*, Volume 277, January 2023.
- D. Gouskos, L. Iannucci, The low velocity impact response of biaxial Non-Crimp Fabric

(NCF) composites with deliberate imperfections: Experimental and numerical study. *Composite Structures, Submitted, 2022*.

### Conference Publications

- D. Gouskos, L. Iannucci, Finite Element Analysis of Non-Crimp Fabric laminates under compact tension. InProc. of ECCM18 - 18th European Conf. on Composite Materials, Athens, Greece 2018 Jun 24.
- D. Gouskos, L. Iannucci, Finite Element Analysis and experimental testing of Non-Crimp compact tension specimens - Influence of cohesive properties. ICCM19-22nd International Conf. on Composite Materials, Melbourne, Australia 2019 Aug 16.

# Chapter 2

## Literature Review

### 2.1 Non crimp fabrics

NCF composites is the type of composites which attempts to combine the excellent in-plane properties of 2D laminates with the improved fracture toughness properties and low manufacturing cost of woven fabrics. They are consisted of fibre tows, usually made of carbon or glass, which assumed to be straight and aligned, stitched together with a knitting yarn either from polyester or aramid (Fig. 2.1), whilst a NCF blanket may be formed by stacking one (UD-NCF), two (biaxial), three (triaxial) or four (quadaxial) layers in order to meet the required fabric properties. The stitches have been developed as a mean to enhance matrix toughness as it has been reported in several studies [14,15] The fibres of each layer are grouped together in parallel tows, constrained with the use of knitting yarn [16] instead of weaving the tows together as it happens in woven blankets. An extensive research for the mechanical properties and numerical modelling of NCF composites for different NCF types, stitching parameters and manufacturing methods was performed through FALCOM project [17–19], many findings of which are presented in the following subsections of this chapter.

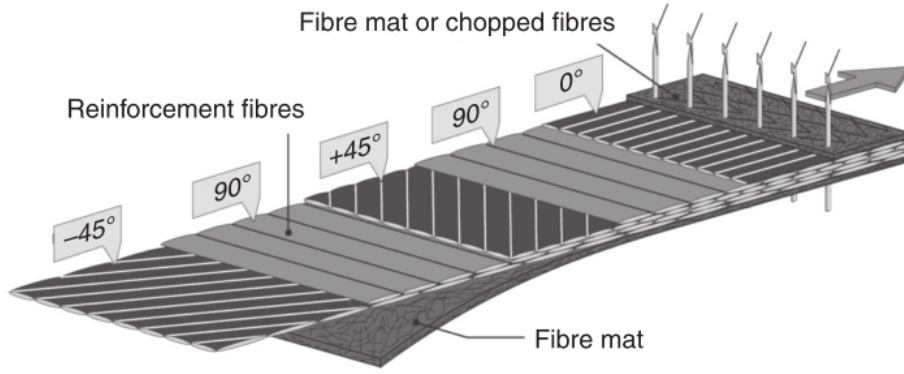


Figure 2.1: *Schematic set-up of a multiaxial warp-knitted non-crimp fabric [4].*

## 2.2 Internal structure of NCF composites

In terms of the internal structure, NCF composites could be classified in-between the UD prepreg (UDPT) and woven composites (Fig. 2.2), and despite the definition of "Non-Crimp", a certain amount of waviness, is present in the fabric both in- and out-of-plane. The stitching parameters, such as stitch length and pitch, are responsible for the in-plane waviness while the out-of-plane fibre waviness is attributed to the imperfections occurring during the manufacturing process. The binding yarn system is usually made of polyester while there are several knitting patterns for binding the fibre tows, namely warp or weft knitted tricot pattern, multi-axial warp-knitted pattern or pillar, tricot and plain stitch patterns (Fig. 2.3). Nevertheless Asp et al. [20] demonstrated that there is no principal difference between composites from NCF with different binder yarn structures in terms of structure performance but the parameter which is mostly affected is drapeability [4]. More recently (2014), Vallons et al. [21] examined the influence of the stitching pattern on the internal geometry and mechanical properties of glass fibre NCF composites by comparing experimentally a new asymmetric stitching pattern to an industrial common type for two identical quasi-unidirectional NCF composites. Interestingly, it was shown that the asymmetric stitching affected negatively the mechanical performance of the NCF composite both in quasi-static and fatigue conditions.

In microscale the fibre tows are described as UD composites with certain fibre content and ho-

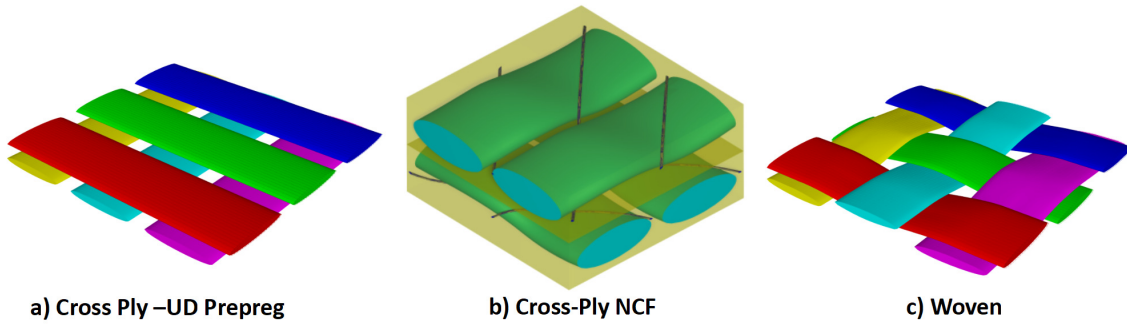
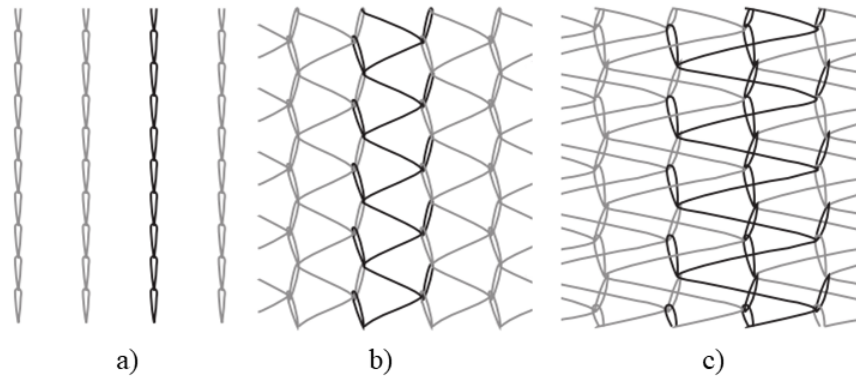


Figure 2.2: a) *Pre-impregnated tape based cross-ply preform*, b) *Cross-ply NCF* and c) *Woven reinforcement*.



Source: ProCad Warpknit 3D ITA

Figure 2.3: *Stitch types a) pillar, b) tricot, c) plain.*

mogenised tow properties. In mesoscale, the fibre tow is assumed as homogeneous transversely isotropic material which interfaces with resin (matrix) and neighbours with other fibre tows (Fig. 2.4). Drapier et al. [22], [23] as well as Edgren et al. [24] presented the complexity of the geometry of fibre tows and also studied the influence of the micro-/mesoscale structure on the mechanical properties of the NCF composites. Throughout the past decades many researchers have been searching for an accurate description of the waviness of fibre tow in NCF. The stitching process introduces artefacts that can influence the final properties of the material, causing for example damage to the brittle fibres. The tension of the stitches against the fibre creates a local footprint and thus an undulation. Backer et al. [25] initially defined the fibre crimp as the relative difference in length between the fibre tow and the fabric.

In 1996, Miller et al. [26] showed that for cross-ply NCF laminates it has been assumed that the waviness of the  $0^\circ$  tows could be represented by a periodic wave due to the periodical distribution of the resin pockets between the  $90^\circ$  tows (Fig. 2.5). The periodicity of the fibre

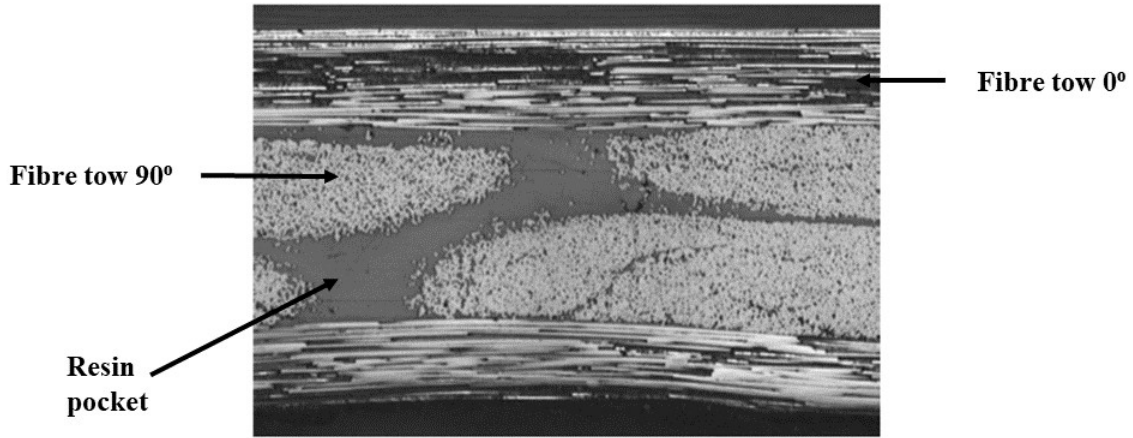


Figure 2.4: Micrograph of a non-crimp fabric cross-ply laminate cross-section [5].

tows, however, is not a certain phenomenon; hence another parameter of imperfection can be used instead of wavelength and amplitude. Such a parameter could be the maximum inclination angle as proposed by Joffe et al. [27]. As mentioned earlier this type of waviness is induced by the stitching footprint in the sense that separates the  $0^\circ$  tows from the adjacent tow in the area closest to the footprint. In Figs. 2.6a) and b) can be easily observed the waviness of fibre tow in the in-plane direction through the angle variation of the fibres of the  $0^\circ$  tow. Edgren et al. [24] suggested to use the measured mean values of the stitch gauge, namely the distance between two adjacent stitch lines for the out-of-plane waviness wavelength. However, this assumption is applicable mainly to pillar stitching than in tricot or plain patterns. Recently, Wilhelmsson et al. [28,29], developed advanced techniques based on image analysis in measuring accurately the out-of-plane fibre waviness in the form of angular misalignment.

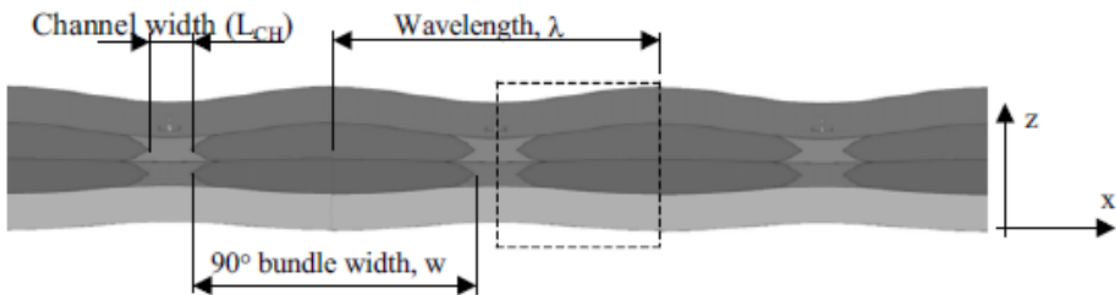


Figure 2.5: Representation of the out-of-plane misalignment of the  $0^\circ$  tow [6].

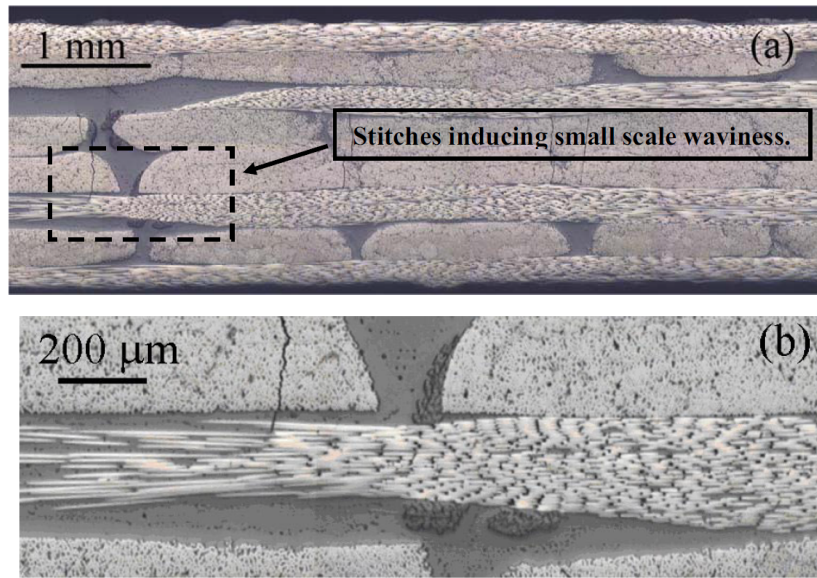


Figure 2.6: a) Mesoscale view of biaxial NCF laminate microstructure, b) focused view on in-plane waviness due to stitch induction [6].

## 2.3 Mechanical properties of NCF composites.

The particular internal structure of NCF composites have a direct effect on their mechanical properties as it has been widely reported by Dransfield et al. [30], [31], Jain et al. [32] and Tsai et al. [33]. In terms of strength, NCF do have significant differences compared to woven and UDPT laminates but in principle they are characterised by improved through-thickness properties without significant drop in in-plane performance, higher out-of-plane fracture toughness and damage tolerance than UDPT laminates.

### 2.3.1 Tension

With regards to the tensile behaviour of NCF composites, the significant influence of the stacking sequence to the stiffness of cross-ply NCF composites in tension was studied by Edgren et al. [34] and Mattsson et al. [35] which identified significant reduction of stiffness for  $[0^\circ/90^\circ/0^\circ/90^\circ]_s$  lay-up. Mattsson and Varna [36] examined the influence of the volume fraction of fibre bundles in the  $90^\circ$  layers of cross-ply NCF laminate to the transverse stiffness and strain. They found out that the higher the bundle volume fraction in the layer the larger the transverse strain in the bundle, and derived exact expressions to calculate the stiffness matrix



of NCF composites.

The tensile strength of NCF composites is generally lower compared to their UDPT counterparts, nevertheless the range of degradation of the strength has been on debate through various research studies from just a few percent (1-3%) [37] to several tens of percent (30-40%) [3] for carbon fibre NCF. There are several causes which could drive in such deviation of the outcome (i.e. damage during the knitting process, different fibre volume fraction between the two types of composites or unforeseen errors during the experiment). Bibo et al. [3], though, states that the fibre volume fraction differed by as little as 6% between the UDPT composite and the NCF; hence the large deviation of the tensile strength cannot be exclusively related with the fibre volume fraction difference.

Attempts to improve the tensile strength of the NCF composites have been made by Arteiro et al. [38], [39] who combined standard grade 0° plies with thin transverse and off-axis plies. The outcome of this experimental study showed that while the tensile performance of the unnotched specimens remained unchanged, for the centre notched and open hole specimens, enhanced tensile strength was observed. The thin ply laminates under tension exhibit an approximately linear behaviour up to failure, indicating that permanent severe damage such as extensive transverse cracking and delamination, was mitigated.

Failure mechanisms due to tension are well defined in NCF as reported by Bibo et al. [3] in a triaxial NCF carbon composite. Another study was performed by Edgren et al. [34] who discussed the results of tensile tests on cross-ply NCF composites and were primarily focused on the formation of damage and the influence of this damage to the composite properties. As it can be seen in Fig. 2.7, damage in form of crack was observed only in layers with 90° orientation of fibre tows. The observed cracks were divided into longitudinal, half and whole cracks. In 2005, Truong et al. [40], presented the development of intra-laminar cracks in NCF cross-ply loading direction, while in the same study with the assistance of x-ray and c-scan analysis it was shown that damage occurs periodically.

Edgren et al. [34] described the influence of the local waviness of NCF in tension with the use of

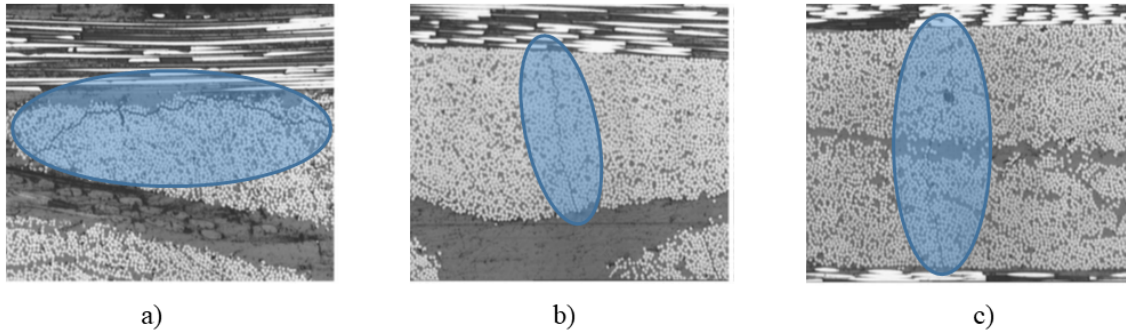


Figure 2.7: *Micrograph of 90° layer a) longitudinal cracks b) half cracks and c) whole crack [7].*

FE methods, while the same approach was investigated with analytical models in [24]. In those studies, it has been shown that the mesoscale waviness in NCF composites is the dominant parameter which affects their mechanical properties. This local fibre misalignment was also studied by several researchers [15, 41, 42] and it was found that it can introduce resin-rich sites or even voidage around the stitches, which are initiation sites for tensile failure and tensile stiffness degradation; as much as 20% [43] in fibre-dominated stacking sequences [42]. Under such loading conditions, the failure will extend across the stitches, like a perforation [44].

Sakai et al. in 2013 [45] studied the relationship between damage initiation and propagation with fabric geometry under static and cyclic tension conditions. The obtained results indicated that there is an impressive mechanical influence of the reinforcement textile in the composite and that damage generation and propagation is highly dependent of the direction in which the stress is applied in relation to the architecture of the fabric. Particularly in cyclic tensile mode, Kaiser [46] effect was observed at low stress, while the experimental results showed that the Felicity effect became clearer along with the increase of stress level.

### 2.3.2 Compression

One of the main disadvantages of NCFs, and needs to be overcome, compared to the UDPT composites, is their lower compressive strength due to the higher waviness of the carbon fibres. The compressive strength of NCF is generally considered lower than for UDPT, but as for the tension case, varying degrees from few percent of improvement (2-10% for triaxial NCF) [37] to 50% decrease of compressive strength have been noted [20] for NCF composites. The failure

mechanism in compression is a formation of kink-bands; it was understood already in 1972 by Argon [47] that this process is driven by matrix-shearing and thus governed by initial fibre misalignments with respect to the laminate and loading direction. Fibres fail into small segments which rotate under increasing load.

Early studies [48–51] conducted on compressive failure of multidirectional composites, with the utilisation of fractography, observed that extensive translaminar cracks (Fig. 2.8a)) had formed in the vicinity of the notch in the majority of the plies (where stress concentration peaks) and were related to the induced delamination. Potter [49] also observed that the off-axis plies affected the behaviour of the axial load-bearing plies and that the translaminar cracks in the axial plies led to in-plane shear fractures at the off-axis plies (Fig. 2.8). The presence of an intralaminar crack at the  $45^\circ$  ply governed the fibre microbuckling at the axial plies and reduced the load carrying capability of the axial plies, i.e. the ply split acted as stress concentration on the  $0^\circ$  layers (Fig. 2.9).

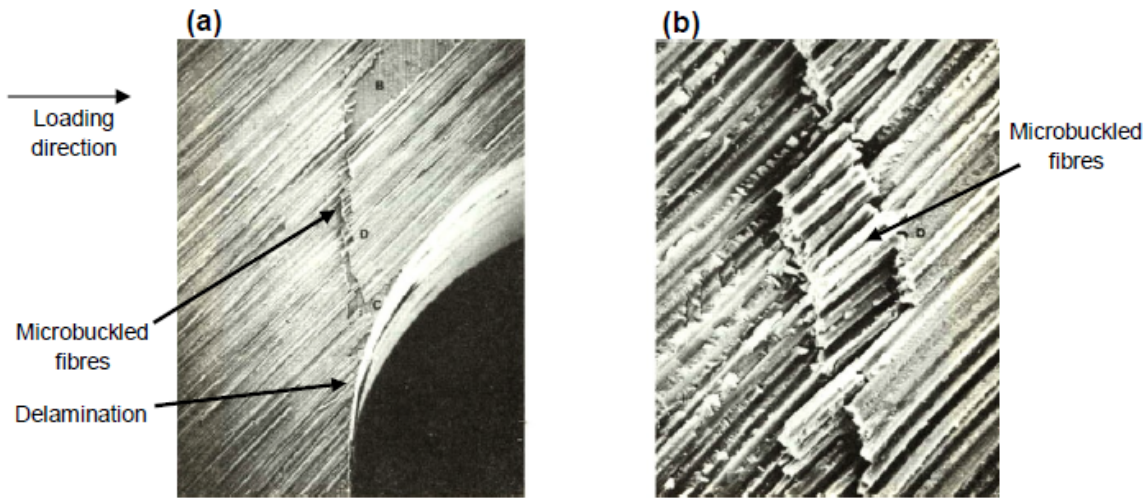


Figure 2.8: a) Fibre fracture in off-axis plies, b) magnification of fracture next to the hole [8].

In one of the latest relevant studies, Wilhemsson et al. [29] provided fundamental understanding of how fibre waviness, fibre volume fraction and laminate thickness influence the compressive stiffness and strength of unidirectional NCF composites. As expected, fibre waviness was found to strongly impair the compressive mechanical properties of the composite. More precisely, the measured compressive strength was reduced to the half as the maximum fibre misalignment angle was doubled. It was also shown in Joffe's [52] research that the average fibre content alone

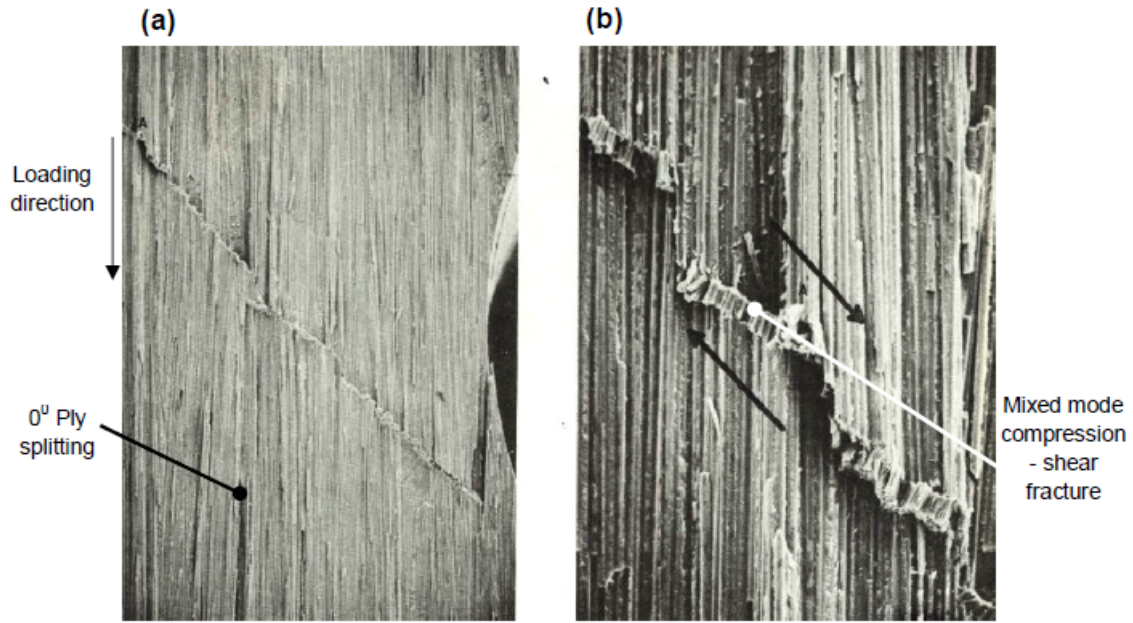


Figure 2.9: a) Buckling failures in axial ply, b) mixed compression and shear failure [8].

does not govern the kink-band formation strain. At fixed average fibre content and mesoscale waviness, the composite with more evenly distributed fibres (lower fibre content in the bundle and larger bundle content in the composite) showed larger resistance to compressive failure.

Similarly, the compressive strength of NCF composites has been reported by several researchers [22,26,29,53] to be 10-50% lower when compared to UDPT and highly influenced by the out-of-plane fibre misalignment [54]. Interestingly, Wilhelmsson et al. [55] noted that the compressive strength is less susceptible to off-axis angles between  $0^\circ$  and  $10^\circ$  while it gradually drops at angles over  $10^\circ$ . Failure mechanisms under compression were initially studied by Argon [47] and identified that the initial fibre misalignment with respect to the laminate and loading direction results to a matrix-shearing failure and thus kink-band formation. Recently, Shipsha et al. [56] investigated the compressive strength and the damage mechanisms of cross-ply NCF composites for different off-axis angles and identified that the governing failure mechanism for all specimens was fibre kinking, with only exception the specimen with  $45^\circ$  off-axis angle where the governing failure mechanisms were shearing cracks within fibre bundles and cracks at bundle-matrix interface. Wilhelmsson et al. [57] explicitly studied the severity of a misalignment region to the longitudinal compressive failure initiation and sequence of the developed failure mechanisms of UD NCF composites and presented an approach which accounts the stochastic nature of these

materials. Interestingly, it was revealed that the compressive strength is influenced by 25% by the area of the misalignment, and the extension ratio is half as influential as the misalignment angle.

### 2.3.3 In-plane shear

More recently, Edgren et al. [58] combined uniaxial compression and shear on NCF specimens. The in-plane shear tests were performed on rectangular  $\pm 45^\circ$  specimens in a compression rig as used for the compression tests. The test results showed a non-linear shear response in function of strain (Fig. 2.10a)) and from fractography kink-band formation was recorded (Fig. 2.10b)). Long Li et al. [59], proceed with a similar study about the in-plane shear behaviour of the carbon fibre NCF composites with chain and tricot-chain stitches and was characterised by picture frame and bias extension tests. In particular, the research was mainly focused on the influence of the stitching on the in-plane shear behaviour of the NCFs. It was found that the stitching yarns' strained condition depending on loading direction influences significantly the shear behaviour of the NCF. The shear test leading to a tensile strain within the stitches can increase the shear resistance by more than 10 times, whereas shear resistance in the direction of compression of the stitching was quite low due to the disturbances of the fibres.

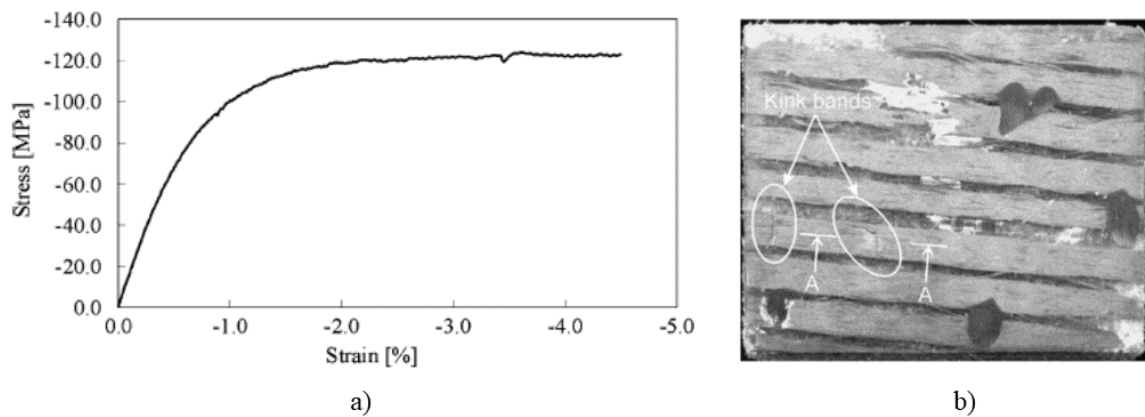


Figure 2.10: a) *Stress - strain curve from in-plane shear stress*, b) *kink-band damage on outer  $45^\circ$  ply* [9].

Godbehere et al. [37] with the use of the short-beam-test, presented a degradation of ILSS performance in NCF composites compared to UDPT between 17-22% for carbon/epoxy. The

degradation of ILSS was reported at even higher levels by Backhouse et al., by reaching the value of 30%. However, the consistency of these results is limited since Bibo et al. [3] reported an increase of the ILSS performance of triaxial NCF composites up to 22%. The results though were surprisingly unexpected for the authors and they assumed that a possible degradation of the resin of UDPT occurred. In 1995 Wang et al. [60] were mainly focused in their research on the type of failure of NCF composites under shear load. The outcome of their investigation was oriented towards a mixed mode type of failure. More specifically, on the top of the specimen, under the loading point, the failure mode was compressive failure; on the bottom face of the specimen the failure modes were tensile failure, in-plane shear failure and delamination. In 1999 Drapier et al. [23] with the application of RVE and Finite Element methods studied the interlaminar shear behaviour of NCF composites in function of the geometrical parameters of an RVE. The result of this research showed that the increase of resin modulus and yield stress improves the ILSS behaviour of an NCF composite, while the increase of resin layer's thickness between the  $0^\circ$  and  $90^\circ$  - layer results in decrease of the ILSS performance.

### 2.3.4 Interlaminar failure

A comparison of the interlaminar strength between NCF composites and UDPT laminates leads to the fact that that NCF do exhibit superior mechanical behaviour. The presence of the stitches is the main reason that not only enhances the interlaminar properties of the laminates under static and impact loads [61, 62] but it also controls crack evolution at delaminated interfaces [63, 64]. The failure mechanisms typical of continuous stitching for a Mode I and II [65, 66] (interlaminar failure due to peel and shear respectively) tests are identified as debonding of the thread-matrix interface, elastic stretching of the thread, and thread failure. The formation of a bridging zone of the threads through-the-thickness and their elastic stretching are the dominant mechanisms thanks to which the interlaminar fracture toughness is increased [31]. Tsai and Chen [67] studied the effect of different types of stitching on the energy release rate through finite element method and compared them against non-stitched laminate. It was found that the interlaminar fracture toughness in peel ( $G_{IC}$ ) is between 3 up to 5.5 times higher than the non-stitched laminate when the stitch reinforcement is made by glass, Para-aramid and

PAN-based carbon fibres.

### 2.3.5 Flexure

Unlike other mechanical properties of the NCF composites, the flexural strength is not extensively documented, hence it is difficult to draw valid conclusions for this property. In one research study by Bibo et al. [3] it was found that the flexural strength of NCF composites is 15% lower than of the respective UDPT laminates. Wang et al. [60] investigated the failure modes due to flexure loading and concluded that brittle failure occurs on the tensile side of specimen and specifically fibre tension failure. This study involved the testing of biaxial, triaxial and quadaxial glass fibre NCF composites. In a similar study, Shiino et al. [68], focused on testing in quasi static mechanical mode (3-point bending) a mono-component resin RTM/carbon fibre anti-symmetric quadaxial fabric stitches by PE 80 Dtex yarn. In this study was reported that dispersive results in flexural strength were observed and a deduction of 10-20% [69] compared to their pre-preg counterparts was recorded.

### 2.3.6 Translaminar fracture

Regarding their translaminar fracture toughness, NCF composites have not been studied as thorough as UDPT [70–74] or woven composites [75, 76]. Due to NCF composites' particular internal structure, it is not always feasible to obtain valid experimental results concerning their translaminar fracture toughness. Research studies [77, 78] have shown that the premature compressive failure is enhanced by fibre misalignment whose presence is commonly observed at NCF composites. Gigliotti and Pinho [79] examined the translaminar fracture toughness for NCF composite of triaxial ( $45^\circ/0^\circ/-45^\circ$ ) blankets, however, they were able to obtain only the translaminar fracture toughness at initiation while due to premature compressive failure of the CT specimens the propagation values were not determined. Though, they noted that the stitching yarns inhibit the propagation of the intralaminar delaminations. Also, in [43] it was mentioned that in notched laminates under tensile loading, the stitches can suspend the development of localised delamination and this can lead to an enhancement in their performance.



### 2.3.7 Impact

Most of the research done to this date concerns the in-plane properties of NCF composites. However, it is not unlikely that a composite part will encounter one or more significant out-of-plane loads during its life cycle. Impact induced damage in composite laminates consists of fibre breakage, matrix cracks and delaminations [80], [81] (see Fig. 2.11). The impact response of composite plates is governed by the impactor/plate mass ratio, where small mass impactors result in a local response controlled by wave propagation and large mass impactors result in a quasi-static response [82, 83]. Olsson et al. [84] presented a general analytic model for large mass impact on plates which was further developed in [85] for small impact response with delamination onset and propagation.

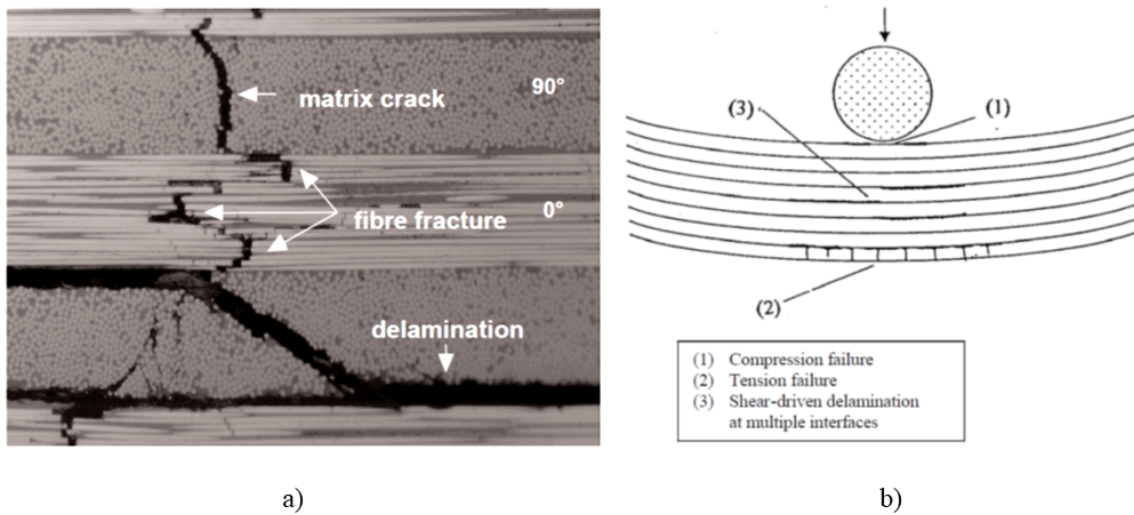


Figure 2.11: *Damage types in impacted fibre reinforced laminates [10].*

In the recent years, a good amount of research has been performed to experimentally characterise the structural and damage response of NCF composites under low velocity impact. Although the basic mechanical properties (e.g. tensile and compressive strength) are in principle lower for NCF composites than for UDPT laminates, the impact resistance properties are improved. The vast majority of the related studies shown that due to the stitch reinforcement and hence the improved interlaminar fracture properties, the delaminated area induced by impact is reduced for low energy levels (i.e., 4 - 10 J) [86, 87]. On the other hand, Aymerich et al. [42, 88] noted in their study that stitching improved the impact damage resistance for



$[0_3/90_3]_S$  laminates, for which the main delamination was sufficiently large to allow the full development of the stitch bridging zone whilst for a  $[0/90]_{3S}$  lay-up no increase in delamination resistance was observed, which exhibit a damage pattern consisting of small overlapping delaminations unable to activate the toughening mechanism introduced by stitching.

Bibo et al. [89] compared NCF laminates with polyester against laminates with aramid stitches, where the latter exhibited reduced levels of matrix cracking for the same energy levels. The impact pattern of NCF composites was described as ‘root-like and tow nesting’, while they observed a minor difference in delamination damage extent between resin film infusion and resin transfer moulding NCF laminates. Also in 2000, Bibo et al. [90] accounted in their research different processing routes on the impact resistance of NCF composites and compared these results to UDPT composites with same lay-up and material properties. It was observed that both configurations had similar response under impact.

In terms of energy absorption several parameters have been studied, such as stitch structure [91], stitch material [92] and fabric architectures [93]. Chen and Hodgkinson [93] compared NCF composite laminates against UDPT and 3D woven fabric composites under low velocity impact and revealed that NCF composites exhibited superior structural integrity, absorbed less impact energy than the other two laminate tapes and their damage mechanisms were characterised by large areas of delamination and matrix cracking even at higher energy levels. In 2010, Vallons et al. [94] compared the impact properties of a biaxial  $\pm 45^\circ$  carbon fibre NCF with a carbon fibre twill weave woven fabric composite for two impact energies (3.5 and 7 J). The projected damage area after impact was larger for the NCF composite than that for the woven fabric composite for both impact energies, resulting in higher energy absorption by 10% for the NCF composite compared to the woven fabric composite. More recently, Saeedifar et al. [95] performed a comparative study between NCF, 2D plain woven and 3D orthogonal plain woven laminates subjected to compression after multiple-impact loading. Concerning the impact response the induced damage area of the NCF composite was 1.5 and 3 times higher than 2D plain woven and 3D orthogonal plain woven laminates respectively.

## 2.4 Manufacturing methods

The most commonly used manufacturing methods for NCF fabrication are based on the liquid moulding technique. In principle, during this method dry NCF preforms are placed into a mould and infiltrated with a liquid thermoset resin. The presence of the stitches can occasionally lead to difficulties in draping and processing of the material since they induce compaction which can inhibit infusion of the resin into the preform [14, 41]. Several different processes are used e.g resin transfer moulding (RTM) or resin infusion under flexible tooling (RIFT) [96]. The advantages of the above-mentioned processes is that they can produce composite laminates out-of-autoclave with low material wastage, reduced production time and virtually unlimited shelf life [97]. The significance of these parameters is high, since they result in a relative low manufacturing cost and easy handling of large sheets of fabric [4].

Bibo et al. [3, 89], studied a rectangular plate aiming to determine the cost savings when switching from UDPT laminates to NCF composites. The outcome of the research was that for UDPT laminates fabrication, labour costs can account for as much as 50% of the final cost, whereas the material cost is 25% of the overall cost. The major part of the cost for labour consists in progressively de-bulking and laying down the material, which can be significantly reduced by using NCF and a resin infusion technique. Since one layer in the NCF composite corresponds to approximately 7 UDPT layers, deposition rates are much higher in NCFs.

NCF composites are manufactured in two main steps; manufacturing of the preforms and moulding of the preform into the final composite. The preforms are made from layers of non-crimped fibre tows stacked in the desired orientation and bounded together by a warp-knitting procedure in which the binder yarns are inserted through the thickness of the perform. Manufacturing parameters associated to both the preform and composite manufacturing are important for the final performance of the composite. During compression inside the mould in the RTM process, the compaction forces which compress the fibre stacks might create nesting and crimp as well as distortion of the cross section shape of the fibre tows larger than in the case of the RFI or RIFT process which use a vacuum bag. As in UDPT laminates, the fibres in

NCF composites should theoretically be straight with only small scale waviness on the micro-scale. However, this is not always the case and certain waviness is normally introduced by the manufacturing procedure at the mesoscale [98].

## 2.5 Numerical modelling of NCF composites

The numerical modelling of NCF composites is very important and challenging and two main approaches are used: a mesoscopic in which the constituents of the NCF composite are modelled by FE methods as a Representative Volume Element, incorporating the heterogeneity of the NCF composite. Such models have been presented in several studies [22–24, 99–103], each study with different assumptions, but able to predict the stiffness and strength of the NCF composite accurately, while the failure mechanisms were also successfully simulated. Nevertheless, this approach is computationally demanding and impractical for implementation in large scale structural components.

In the second method, the modelling of the NCF composite is employed at the macroscopic level where classical laminate theory (CLT) is used for the analysis. In this approach the composite consisted of homogenised layers of different orientations assuming evenly distributed fibre tows, whilst the mechanical properties at layer level are obtained either from the rule of mixtures (ROM) or analytical multiscale methodologies [104, 105]. The internal structure, namely fibre undulation, is introduced usually as a local coordinate system at an element level or as a sinusoidal function assuming a periodicity in fibre waviness.

### 2.5.1 Longitudinal Stiffness

At mesoscale (layer) level the defects in the NCFs (as, for example, the waviness of the tows) need to be considered in order to correctly simulate their overall mechanical performance. Drapier et al. [22, 23] assumed sinusoidally shaped crimp, defined by its wavelength ( $\lambda$ ) and amplitude which is related to the maximum misalignment angle represented by the standard deviation of the orientation (SDO) with respect to the theoretical direction ( $\phi_{\max}$ ), Eq. 2.1 (see

Fig. 2.12).

$$\phi_{max} = \pm\sqrt{2}SDO \quad (2.1)$$

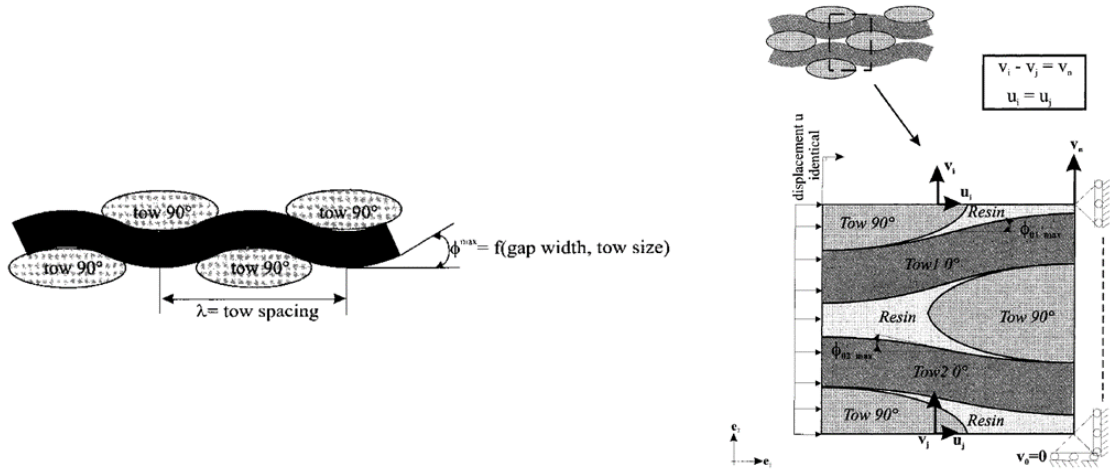


Figure 2.12: *Schematic of bi-dimensional repeating cell: geometry, parameters and boundary conditions [11, 12].*

Edgren et al. [24] developed a mathematical model based on Timoshenko's beam theory applied on curved beams, representing wavy tows in NCF composite layer. The theoretically determined knock-down factors operating at layer level were introduced to laminate theory. The model was validated by comparison between predicted and experimental laminate constitutive properties as well as with numerical results of cross-ply laminate. Fig. 2.13 shows the approach defining the stiffness knock-down factor,  $\eta$ , for a wavy layer related to that of a straight as  $\eta = S_1/S^0$ ,  $S_1$  and  $S_0$  are the axial stiffness of a curved and a straight beam respectively. The tow waviness is based on the sine shaped out of the layer plane, as in [22, 23] but developed to the form of Eq. 2.2:

$$z = A \cdot \sin\left(\frac{2\pi}{\lambda}x\right) \quad (2.2)$$

where,  $\lambda$  is the wave length of the crimp and  $A = \sqrt{2} SDO$  is the amplitude of the wavelength.

In 2006, Tessitore et al. [99] represented the out-of-plane tow waviness in sinusoidal shape, but the cosine function was found to be rather representative for the wave shapes and developed the expression of the tow waviness as described by Eq. 2.3:

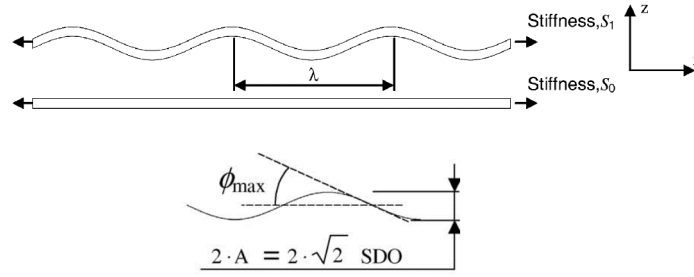


Figure 2.13: Schematic of tow waviness as developed by Edgren et al. [13]

$$z(x) = \left(a_1 + \frac{a_2}{2} - \frac{c}{2}\right)A \cdot \cos\left(\frac{2\pi}{\lambda}x\right) \quad (2.3)$$

where,  $\lambda$  is the wavelength,  $c/2$  is the wave amplitude and  $z, x$  are the coordinates of the tow for plane  $y=0$ .

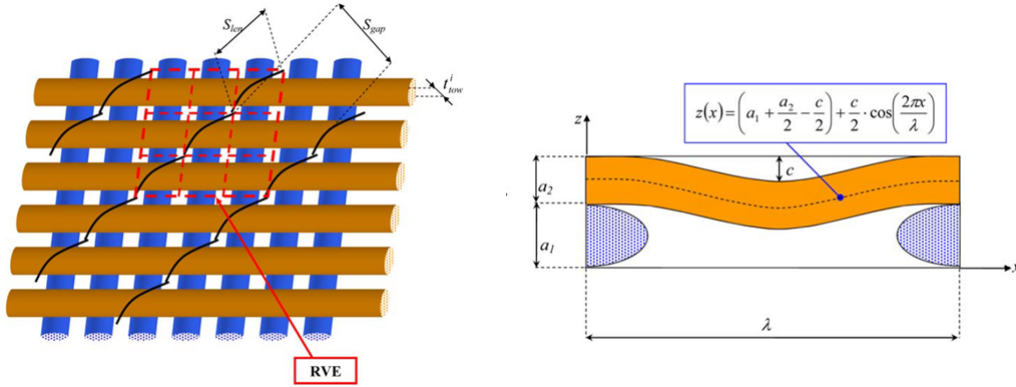


Figure 2.14: RVE for a biaxial preform with chain stitch pattern and waviness of bundles [14].

The developed tool by [99] was found to be effective in predicting the mechanical performance of biaxial NCF composite structures under tension. Similarly, in 2011, Petriccione et al. [106] developed their method again in the averaging stiffness approach, taking into further account matrix porosity.

The study of Oakeshott et al. [100] was focused on a biaxial  $\pm 45^\circ$  NCF blanket, which included potential variability in the NCF structure. The induced crimps due to the stitches were simulated by shape functions in each of the local  $x$ -direction at the centre of the tow layers, which have the form of cosine distributions with negative sign in the case of  $+45^\circ$  tow layers and positive for  $-45^\circ$ . The detailed forms of the shape functions  $H_{+45}$ , for the  $+45^\circ$  tow layer and,  $H_{-45}$ , for the  $-45^\circ$  tow layer are shown in Eq. 2.4.

$$H_{+45}(x, y) = \frac{h_t - h_c + h_1}{2} - \frac{h_c}{2} \cos\left(\frac{2\pi(x + y)}{p}\right) = -H_{-45}(x, y) \quad (2.4)$$

where,  $h_t$  is the height of the tow layers,  $h_c$  is the height of the crimp in the tow layers,  $h_1$  is height of one inter-ply resin layer, and  $p$  are the coordinates in x- and y- directions. The crimp angles,  $\theta_{+45}$  and  $\theta_{-45}$ , were determined from the arctangent of the gradient in the shape functions Eq. 2.4 and described by Eq. 2.5:

$$\theta_{+45}(x, y) = \arctan\left(\left(\frac{\pi h_c}{p}\right) \cdot \sin\left(\frac{2\pi(x + y)}{p}\right)\right) = -\theta_{-45}(x, y) \quad (2.5)$$

while for small  $\theta$  angles they receive the form of Eq. 2.6:

$$\theta_{+45}(x, y) = \left(\frac{\pi h_c}{p}\right) \cdot \sin\left(\frac{2\pi(x + y)}{p}\right) = -\theta_{-45}(x, y) \quad (2.6)$$

The basic formulation of a two-dimensional analytical model is described in Eqs. 2.4-2.6 and the use of Halpin-Tsai equations to predict the through-the-thickness properties were proposed. Two- and three-dimensional models were compared and related to the engineering properties predicted using simple mechanics of materials methods. They observed that the longitudinal and shear moduli calculated assuming the Halpin-Tsain equations for the determination of the through-the-thickness moduli of the  $\pm 45^\circ$  plies were moderately higher than the values calculated assuming parallel expressions.

Zrida et al. [107] highlighted the fibre waviness of the  $0^\circ$  layers as a determinant factor for the longitudinal stiffness degradation and developed a master curve approach to predict it. Earlier, Cox and Dadkash [108] introduced an analytical knock-down factor methodology to model the axial stiffness degradation. However, none of these approaches explicitly distinguished the influence of the in- and/or out-of-plane waviness to the longitudinal stiffness reduction.

### 2.5.2 Failure criteria for NCF composites

Failure prediction, however, is far more complicated due to the different damage mechanisms occurring until failure onset, particularly in fibre kinking [56] and matrix failure. In comparison to the variety of failure criteria, developed for UDPT, limited research has been performed until now for the generation of failure criteria specifically for NCF composites. Edgren [58] assumed a linear interaction between the longitudinal compressive stress and shear stress to predict the strength of a layer. Although, the constitutive model achieved a good agreement with experimental results for quasi-isotropic NCF composites, it required a high number of input data for every layer of the configuration.

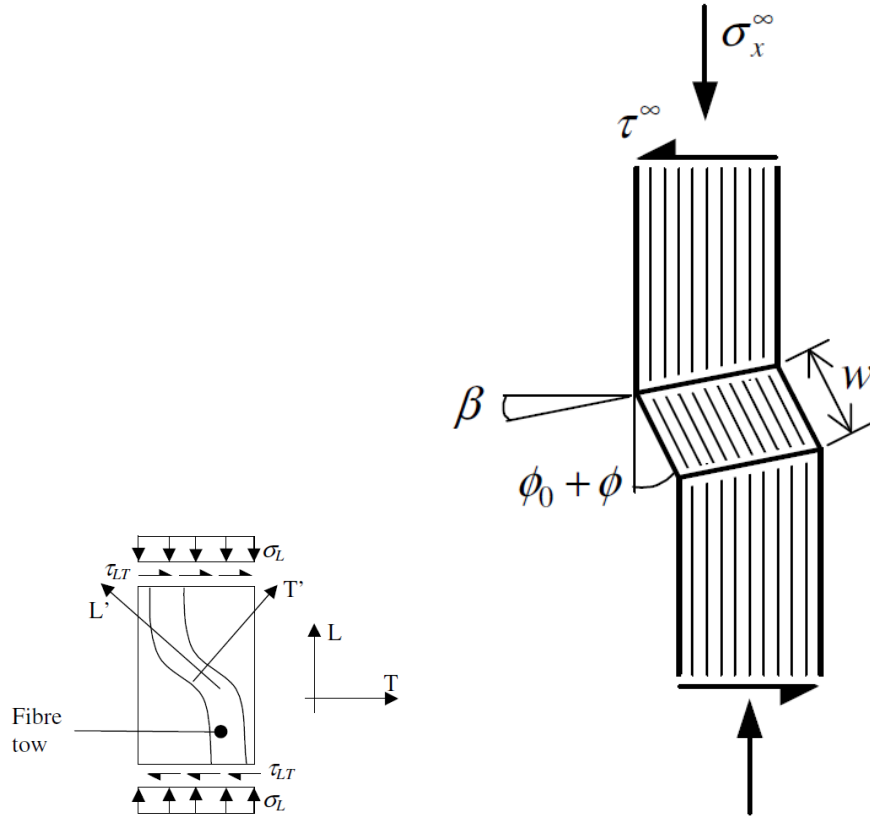
Furthermore, Edgren et al. [58] also studied the behaviour of NCF composites under combined compression and shear loading and an approximate analytical constitutive model for NCF composites was developed. A failure criterion for prediction of failure caused by kinking under multiaxial (axial compression and shear) loading has been adapted to a NCF composite system. The test results proved that the criterion works well for predictions of kinking governed failure for the examined material system. As in the study by Joffe et al. [52] the criterion in Eq. 2.7 is applied to the composite material within the fibre tows in the L'-T' system. A schematic of a fibre tow in a NCF composite unit is shown in Figs. 2.15a) and 2.15b).

$$\sigma_x^\infty = \frac{\alpha\tau_y - \tau_{xy}^\infty}{\phi_0 + \phi} \quad (2.7)$$

where,  $\sigma_x^\infty$  is the remote longitudinal stress in the composite,  $\tau_y$  is the shear yield strength of the rigid-perfectly plastic solid,  $\tau_{xy}^\infty$  is the remote shear stress in the composite,  $\phi$  is the additional fibre rotation caused by the remote loading,  $\alpha = \sqrt{1 + R^2 \tan^2 \beta}$ ,  $R = \sigma_{Ty} / \tau_y$ ,  $\beta$  is the kink band angle, and  $\sigma_{Ty}$  is the yield stress in pure transverse tension.

Assuming linear elastic response, Eq. 2.7 is rearranged to instead be applicable in the lamina coordinate system, L-T:

$$\frac{\sigma_L}{\sigma_{c0}} + \frac{\tau_{LT}}{\tau_{LT0}} = 1 \quad (2.8)$$



(a) Schematic of a NCF composite unit with crimped fibre tow [39]. (b) Schematic kink band form of a unidirectional composite [9].

where,  $\sigma_L$  is the normal stress acting on the NCF composite lamina,  $\tau_{LT}$  is the shear stress acting on the NCF composite lamina,  $\sigma_{c0}$  is the uniaxial compressive strength of the NCF lamina and  $\tau_{LT0}$  is the shear strength of the NCF lamina. Due to the complex micro-structure of NCF composites, Finite Element models have been extensively used to investigate their mechanical response at different length scales [22, 104]. Physically-based failure criteria for UD laminates [109, 110] have been presented particularly in the WWFE [111], and some are proposed for NCF composites [112, 113]. They may come from micro-mechanical derivations or phenomenological investigations and meant to describe the mechanisms involved with failure to give accurate predictions of strength. This type of failure criteria is typically defined by matrix and fibre failure; can predict failure at layer-level and require, as input data, homogenised layer properties which can be measured from standard tests. In order these failure criteria to have practical use, the interaction between matrix and fibre has to be coupled. However, these criteria have not gained a general acceptance until present time, due to the required high number of input parameters and to the fact that they do not account for the transverse orthotropy; still



though they are preferred for the computational analysis of NCF composites. In the following subsections of this chapter the fundamental criteria and some further developments of these criteria are described.

### 2.5.2.1 Hashin's failure criteria

Hashin failure criteria [114] (1980) are simple to use and provide a first step towards phenomenological models and consider four distinct modes, i) Compressive fibre failure, ii) Tensile fibre failure, iii) Compressive matrix failure and iv) Tensile matrix failure. The failure criteria are established in terms of quadratic stress polynomials which are expressed in terms of the transversely isotropic invariants of the applied average stress state. Although the Hashin criteria were developed for unidirectional laminates, they have also been applied to progressive failure analysis for laminates by using in-situ unidirectional strengths to account for the constraining interactions between the plies [115]. The failure modes in a 3D stress-state are presented below:

- **Tensile fibre mode**

$$\left(\frac{\sigma_{11}}{X_T}\right)^2 + \frac{1}{S^2} \cdot (\sigma_{12}^2 + \sigma_{13}^2) = 1 \quad (2.9)$$

where,  $\sigma_{11}$  is the nominal stress in the lamina in fibre direction,  $\sigma_{12}$  is the nominal shear stress in plane 1-2 of the lamina,  $\sigma_{13}$  is the nominal shear stress in plane 1-3 of the lamina,  $X_T$  is the longitudinal tensile strength and  $S$  is the shear strength.

- **Compressive fibre mode**

$$|\sigma_{11}| = X_C \quad (2.10)$$

where,  $X_C$  is the longitudinal compressive strength.

- **Tensile matrix mode ( $\sigma_{22} + \sigma_{33} > 0$ )**

$$\frac{1}{Y_T^2} (\sigma_{22} + \sigma_{33})^2 + \frac{1}{S_T^2} \cdot (\sigma_{23}^2 - \sigma_{202} \cdot \sigma_{33}) + \frac{1}{S_L^2} \cdot (\sigma_{12}^2 + \sigma_{13}^2) = 1 \quad (2.11)$$

where,  $\sigma_{22}$  is the nominal stress in the lamina in matrix direction,  $\sigma_{33}$  is the nominal stress in the lamina through-the-thickness,  $\sigma_{23}$  is the nominal shear stress in plane 2-3 of the lamina,  $Y_T$  is the tensile strength in the transverse direction,  $S_L$  is the shear strength along the fibre direction and  $S_T$  is the shear strength transverse to the fibre direction.

• **Compressive matrix mode ( $\sigma_{22} + \sigma_{33} < 0$ )**

$$\begin{aligned} \frac{1}{Y_C} \cdot \left[ \left( \frac{Y_C}{2 \cdot S_T} \right)^2 - 1 \right] (\sigma_{22} + \sigma_{33}) + \frac{1}{4S_T^2} \cdot (\sigma_{22} + \sigma_{33})^2 + \\ + \frac{1}{S_T^2} \cdot (\sigma_{23}^2 - \sigma_{22} \cdot \sigma_{33}) + \frac{1}{S_L^2} \cdot (\sigma_{12}^2 + \sigma_{13}^2) = 1 \end{aligned} \quad (2.12)$$

Hashin failure criteria do not always fit the experimental results [116, 117], especially in the case of matrix or fibre compression. It is well known that moderate transverse compression ( $\sigma_{22} < 0$ ) increases the apparent shear strength of a ply, which is not well predicted by Hashin's criterion. More significantly, for matrix compression, the angle of the plane of fracture is not calculated by the model. In addition, Hashin's fibre compression criterion does not account either the effects of in-plane shear or any type of fibre misalignment, which reduce significantly the effective compressive strength of a ply. Sun et al. [118] proposed an empirical modification to Hashin's criterion for matrix compression failure to take into account the beneficial role that compressive stress ( $\sigma_{22}$ ) has on matrix shear strength. The criterion is:

$$\left( \frac{\sigma_{22}}{Y_C} \right)^2 + \left( \frac{\sigma_{12}}{S_L - \eta \cdot \sigma_{22}} \right)^2 = 1 \quad (2.13)$$

where,  $\eta$  is the internal material friction and  $S_L - \eta \cdot \sigma_{22}$  is the effective longitudinal shear strength that increases with transverse compression ( $\sigma_{22}$ ).

### 2.5.2.2 Puck's failure criterion

Puck et al. [119] used the same idea that Hashin explored in 1980, considering that the failure of the matrix throughout a plane is controlled by the components of the stress vector associated with the plane. The failure criterion proposed by Puck for matrix failure was the first to use Mohr-Coulomb theory for brittle materials and is one of the best attempts to combine phenomenological observations with thorough understanding of the behaviour of composite materials during fracture. The criterion was later adopted in the LaRC failure criteria as it will be discussed later in the thesis. A key element to Puck's proposal is the calculation of the angle of the fracture plane.

The inclination (and location) of the fracture plane are not known in advance. Considering failure of a unidirectional ply, six different loading types, each with its associated corresponding failure strength are distinguished: i) Longitudinal tension, ii) Longitudinal compression, iii) Transverse tension, iv) Transverse compression, v) Shear 12 or 13 loading with the fibres aligned with the 1 direction and vi) Shear 23 loading with the fibres still in the 1 direction. Fig. 2.16 presents the axis system of the laminate.

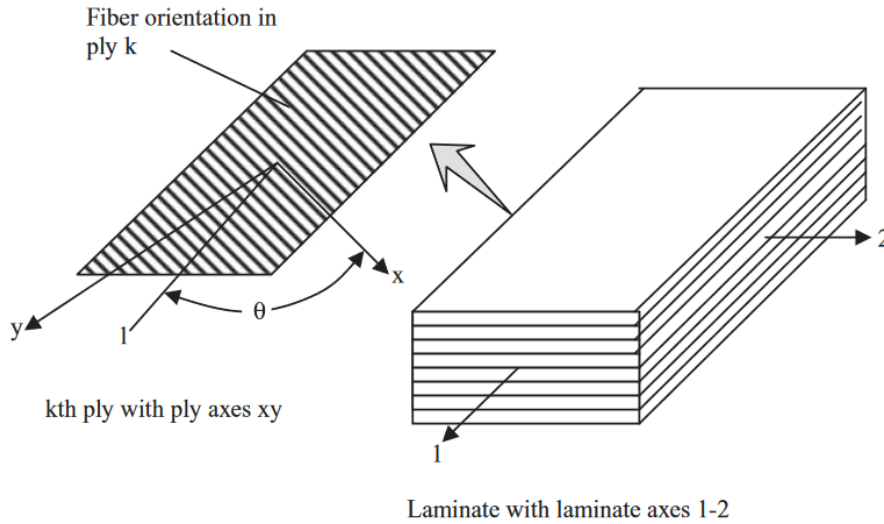


Figure 2.16: *Laminate and ply axes systems.*

Focusing on the individual constituents, the following failure conditions were established:

- **Tensile fibre mode**

The tensile fibre failure mode was originally described by the maximum stress criterion as:

$$\frac{\sigma_{11}}{X_T} = 1 \quad (2.14)$$

Nevertheless, for the contribution to the World Wide Failure Exercise (WWFE) the criterion was developed [120] in order to be more physically correct in the following form:

$$\frac{E_{11}}{X_T} \cdot \left[ \epsilon_{11} + \frac{\nu_{f12}}{E_{f11}} \cdot m_{\sigma f} \cdot \sigma_{22} \right] = 1 \quad (2.15)$$

where,  $E_{11}$  is the longitudinal Young's modulus,  $\epsilon_{11}$  is the strain in fibre direction,  $E_{f11}$  is the Young's modulus of the fibres,  $\nu_{f12}$  is the Poisson's ratio of the fibres in plane 1-2 of the lamina and  $m_{\sigma}$  is the stress magnification factor.

- **Compressive fibre mode**

Similarly, the compressive fibre failure mode was originally described by the maximum stress criterion as for Hashin's criteria but for the WWFE Puck's was developed:

$$\frac{E_{11}}{X_C} \cdot \left[ \epsilon_{11} + \frac{\nu_{f12}}{E_{f11}} \cdot m_{\sigma f} \cdot \sigma_{22} \right] = -1 \quad (2.16)$$

- **Tensile matrix mode ( $\sigma_{22} > 0$ )**

The failure criteria for transverse fracture are based on the modified Mohr-Coulomb criterion, which has been adapted to transversally isotropic material.

$$\sqrt{\left(\frac{\tau_{12}}{S_{12}}\right)^2 + \left(1 - s_{nnL} \frac{Y_T}{S_{12}}\right)^2 \cdot \left(\frac{\sigma_{22}}{Y_T}\right)^2} + s_{nnL} \frac{\sigma_{22}}{S_{23}} = 1 \quad (2.17)$$

where,  $\tau_{12}$  is the nominal shear stress in plane 1-2 of the lamina,  $S_{12}$  is the shear strength in plane 1-2 of the lamina,  $S_{23}$  is the shear strength in plane 2-3 of the lamina,  $s_{nnL}$  is the slope of the failure envelope evaluated with  $\sigma_n = 0$ , when  $\sigma_n$  and  $\tau_{nL}$  are the stresses acting on the failure plane and  $n$  is the normal direction to that plane.

- **Compressive matrix mode with fracture plane perpendicular to the loading** ( $\sigma_{22} < 0 \leq |\sigma_{22}/\tau_{12}| \leq R/S_c$ )

$$\sqrt{\left(\frac{\tau_{12}}{S_{12}}\right)^2 + \left(s_{nnL} \frac{Y_T}{S_{12}}\right)^2} + s_{nnL} \frac{\sigma_{22}}{S_{23}} = 1 \quad (2.18)$$

with  $R = \frac{Y_C}{2(1+s_{nnT})}$  and  $S_C = S_{12}\sqrt{1+2s_{nnT}}$  where,  $s_{nnT}$  is the slope of the failure surface at  $\sigma_n = 0$ , when  $\sigma_n$  and  $\tau_{nT}$  are the stresses acting on the failure plane with  $n$  normal direction to that plane.

- **Compressive matrix mode with fracture plane at an angle to the loading** ( $\sigma_{22} < 0 \leq |\tau_{12}/\sigma_{22}| \leq S_c/R$ )

$$-\frac{Y_C}{\sigma_{22}} \left[ \left( \frac{\tau_{12}}{Y_C} \frac{R}{S_{12}} \right)^2 + \left( \frac{\sigma_{22}}{Y_C} \right)^2 \right] = 1 \quad (2.19)$$

Equations 2.17, 2.18 and 2.19 refer to different matrix failure modes and account for the, usually non-linear, effect of matrix cracking on the final failure.

### 2.5.2.3 LaRC failure criteria

LaRC failure criteria, which adopted Puck's and Mohr-Coulomb's criteria, are a set of physically based criteria for fibre and matrix fracture developed at NASA Langley Research Centre. In principle, LaRC, are based on Puck's criterion for matrix failure and fibre kinking. In the following paragraphs the versions of LaRC are briefly described.

The first version of LaRC criteria was LaRC02 [121], a non-empirical set of criteria for predicting first ply failure of UD fibre reinforced polymer laminates in plane stress state, derived from Puck's action plane concept [120]. The novelty of LaRC02 was the introduction of a new matrix shear and compression failure mode. Moreover, a fibre kinking failure criterion for fibre compression was developed by applying the matrix failure criterion to the configuration of the kink. Matrix cracking and fibre failure in tension were the same as in Hashin's failure criteria [114]. Predictions using the LaRC02 criteria correlated well with experimental results [122].

The failure propagation in LaRC02 was modelled by associating the elastic properties to three user-defined field variables  $FV1$ ,  $FV2$  and  $FV3$ . The first field variable represents matrix failure in tension, the second matrix failure in compression and the third fibre failure. Three degradation factors  $D_t$ ,  $D_s$  and  $D_f$  were introduced using the value of transverse modulus ( $E_{22}$ ), the shear moduli ( $G_{12}$ ,  $G_{13}$ ,  $G_{23}$ ) and longitudinal modulus ( $E_{11}$ ) at the saturation crack density.

LaRC03 [123] is derived from Dvorak's fracture mechanics analyses of cracked plies [124] and Puck's action plane concept, as LaRC02, and can predict matrix and fibre failure accurately without requiring curve-fitting parameters. For matrix failure under transverse compression, the fracture plane is calculated by maximising the Mohr-Coulomb effective stresses. Fibre kinking criterion is obtained by calculating the fibre misalignment under load and applying the matrix failure criterion in the coordinate frame of the misalignment.

Compared to LaRC02, the matrix failure index under transverse compression in LaRC03 is a quadratic expression of the effective stresses but the in-situ longitudinal shear strength is taken into account instead of the strength of a UD laminate. In the matrix failure index under transverse tension a linear term of the transverse normal stress has been added to the initial quadratic expression, in order to represent the in-situ effect occurring in laminated composites. The fibre tension failure index is the same as in LaRC02, whereas for fibre compression failure by formation of a kink band, the in-situ longitudinal shear strength is included, and for fibre compression failure with matrix tension, a linear term of the transverse stress has been added to the existing quadratic formulation.

In version #4 of LaRC failure criteria [125], the LaRC03 plane stress criteria are extended to account general three-dimensional loading and in-plane shear non-linearity. Matrix compressive failure is addressed with a modified 3D version of Puck's matrix compression failure criterion. For the fibre failure in compression, a 3D criterion based on Argon's [47] approach considering matrix failure prompted by material imperfections is used in a framework similar to the one proposed in LaRC02 and LaRC03. A failure model for matrix in tension and shear is derived from Dvorak and Laws's [124] fracture mechanics analyses of cracked plies, as in LaRC03. Recently, Shipsha et al. [126] presented a failure criterion for intra-laminar fibre kinking and

interlaminar delamination based on LaRC04 through which the effect of the stacking sequence on strength and failure mechanisms for various cross-ply NCF lay-ups under compression was studied and appeared to have a discrepancy of approximately 25% against test results.

The LaRC05 version [127], is considered as a state-of-the-art for failure prediction in laminated composites. Regarding its background, it is a pressure-dependent three-dimensional law to predict failure for laminated composites [127] and failure propagation takes into consideration the fracture energy associated with each failure mode and the accumulation of cracks in the plies. As it is stated by Pinho et al. [127] this particular ‘model is employed to make blind predictions of the triaxial failure envelopes’. The current damage propagation model predicts the response of laminates from first ply failure until final laminate failure.

### • Polymer Failure

The failure model at ply level, in terms of polymer failure, is based on the Raghava [128] criterion:

$$FI_p = \frac{3 \cdot (k^2 - (T - C) \cdot (\sigma_h))}{T \cdot C} \quad (2.20)$$

where  $T$  is the tensile strength of the resin,  $C$  is the compressive strength of the resin,  $\sigma_h$  is the hydrostatic stress and  $k$  is a variable defined by the principal stresses.

### • Matrix Failure

Matrix failure criterion, is analogous to Raghava’s and it is an adaptation of Mohr-Coulomb failure criterion for UD composite plies. Matrix cracking without fibre kinking will occur mainly for loading in the transverse 2-3 plane. This type of failure is predicted with Eq. 2.21, where failure occurs for a value greater than one. The failure index for matrix failure is defined as:

$$FI_M = \left( \frac{\tau_T}{S_{is}^T - \eta_T \cdot \sigma_N} \right)^2 + \left( \frac{\tau_L}{S_{is}^L - \eta_L \cdot \sigma_N} \right)^2 + \left( \frac{\langle \sigma_N \rangle_+}{Y_{is}^T} \right)^2 \quad (2.21)$$

where,  $Y_{is}^T$ ,  $S_{is}^L$  and  $S_{is}^T$  are the in-situ transverse tensile strength, longitudinal shear strength and transverse shear strengths respectively.  $\tau_L$ ,  $\tau_T$  and  $\sigma_N$  are the traction components in the fracture plane. The friction coefficients  $\eta_T$  and  $\eta_L$  are introduced to account the effect of the pressure on the failure response.

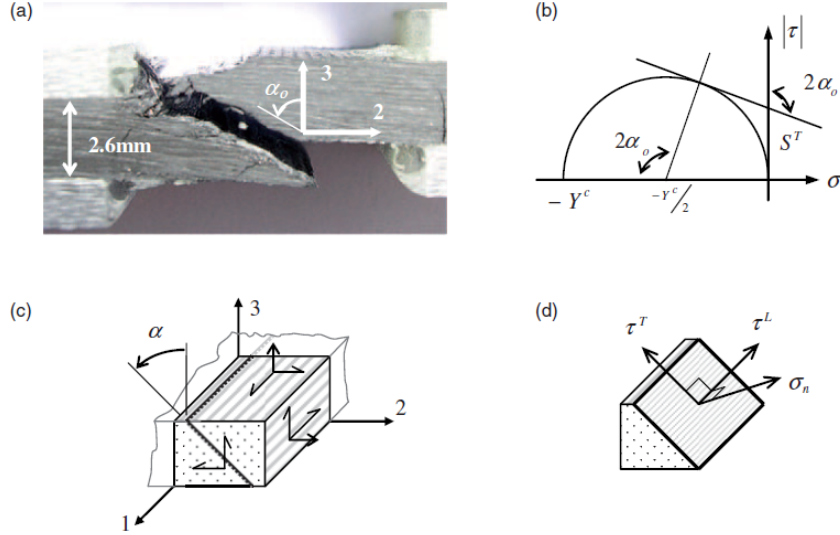


Figure 2.17: Traction components acting on the matrix fracture plane [15].

A compressive failure from load in the 2-direction is shown in Fig. 2.17. The first two terms compare the shear stresses to the measured strengths in the corresponding planes. The last term with the Macauley bracket is only acting when the normal stress to the fracture plane is positive, where it compares this stress to the allowed transverse tensile stress,  $Y_T$ . The strength is increased when compressive normal stresses on the fracture plane  $\sigma_N$  are multiplied with the frictional constants  $\eta$ . The physical motivation is that a compressive stress closes micro cracks in the brittle matrix and generate frictional forces. This action improves the strength of the composite, while opening the cracks has a negative effect which is modelled with the third term in Eq. 2.21.

### • Fibre Kinking and Splitting

The criteria proposed for fibre kinking and for splitting use the same failure index equation:

$$FI_{KINK} = FI_{SPLIT} = \left( \frac{\tau_{23}^m}{S_{is}^T - \eta_T \cdot \sigma_2^m} \right)^2 + \left( \frac{\tau_{13}^m}{S_{is}^L - \eta_L \cdot \sigma_2^m} \right)^2 + \left( \frac{\langle \sigma_2^m \rangle_+}{Y_{is}^T} \right)^2 \quad (2.22)$$



where,  $\sigma_2^m$ ,  $\tau_{12}^m$  and  $\tau_{23}^m$  are the stresses for the subsequent rotation to the misalignment frame.

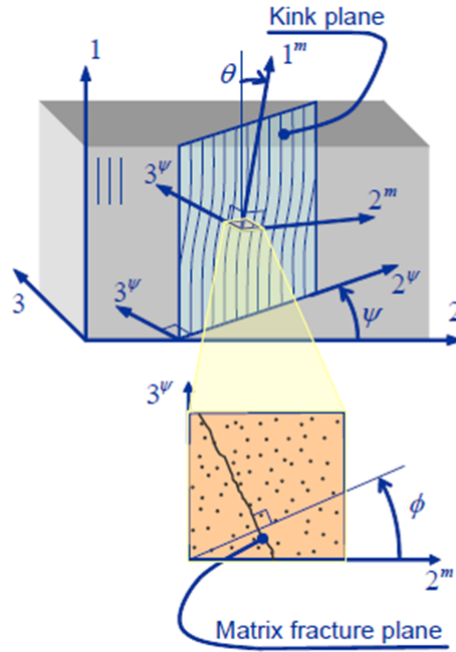


Figure 2.18: *Physical model of kink band formation [15, 16].*

The main difference between fibre kinking and matrix cracking is that applied stresses have a superscript  $m$ , which indicates a transformation to a misaligned coordinate system. Initially, a potential kink-plane is found, similar to Fig. 2.17d) but typically denoted  $\psi$ . The first term of Eq. 2.21 is negligible in the case of longitudinal compressive loading since shear stress in the transverse plane  $\tau_T$  will be small. Fibre kinking has a transition to matrix splitting for large  $\phi_0$ . The LaRC criteria state that fibre kinking occurs if  $\sigma_{11} < -X_C/2$  and matrix splitting if  $-X_C/2 < \sigma_{11} < 0$ .

#### • Fibre Tensile Failure

Fibre tensile failure is expressed similarly to LaRC04 with the maximum stress failure criterion. With regards to damage propagation, this is initiated when the value of the failure index reaches 1. Then the components of the traction acting on the predicted fracture plane are reduced to zero; in such way that the energy dissipation predicted by the numerical model per unit area of the created surface is equal to the fracture toughness of the material. The evolution of the failure is dependent on the damage variable,  $d$  which defines how much energy is dissipated by damage.

The ability of failure prediction for LaRC05 is quite high. In the second World Wide Failure Exercise [129] the predictions of the failure criteria showed very good agreement with the test cases. Nevertheless, LaRC05 assumes transverse isotropy and cannot capture the orthotropic nature of transverse failure observed for NCF composites. Molker et al. [130] presented two failure criteria for transverse failure in NCF composites. The proposed failure criteria were based on LaRC05 criteria and modified to take into account the orthotropic properties of the NCF composites. Specifically, Molker's model considers that the transverse strength of the ply is dependent on the direction of the load.

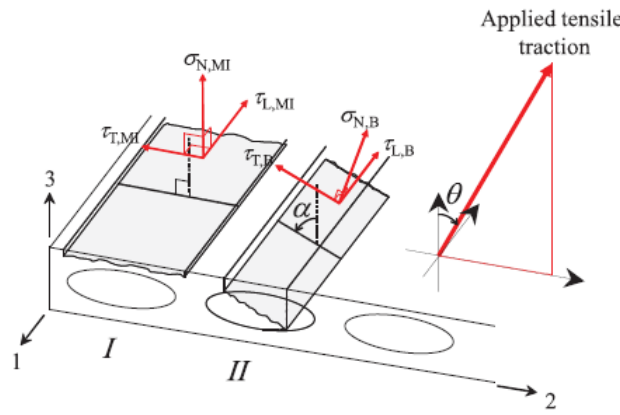


Figure 2.19: *Fracture planes within a NCF lamina. Interbundle failure at bundle I, with the fracture plane parallel to the NCF layer in the matrix. Intrabundle failure at bundle II, with fracture plane inside the fibre bundle [17].*

The first criterion evaluates the stress components against the failure on a plane normal to the layer thickness (Fig. 2.19 bundle I), independently of how the stress is applied. On this fracture surface (MI), between bundle and matrix, the out-of-plane strength,  $Z_T$ , is taken into consideration.

$$FI_{M,MI} = \left( \frac{\tau_{T,MI}}{S_{T,MI}} \right)^2 + \left( \frac{\tau_{L,MI}}{S_{L,MI}} \right)^2 + \left( \frac{\sigma_{N,MI}}{Z_T} \right)^2 = 1, \quad \text{if } \sigma_N > 0. \quad (2.23)$$

where the shear strength  $S_{T,MI}$  and  $S_{L,MI}$  are equal to the interlaminar shear strength. The second criterion for failure within the fibre bundle, corresponding to the original LaRC05 equation 2.21, is denoted with the subscript B (Fig. 2.19 bundle II).

$$FI_{M,B} = \left( \frac{\tau_{T,B}}{S_T - \eta_T \cdot \sigma_N} \right)^2 + \left( \frac{\tau_{L,B}}{S_L - \eta_L \cdot \sigma_N} \right)^2 + \left( \frac{\langle \sigma_{N,B} \rangle_+}{Y_T} \right)^2 = 1 \quad (2.24)$$

where the shear strength  $S_T$  is based on  $Y_C$  as:

$$S_T = Y_C \cdot \cos(a_0) \cdot \left( \sin(a_0) + \frac{\cos(a_0)}{\tan(2a_0)} \right) \quad (2.25)$$

Hence, the transverse failure is evaluated as the maximum of  $FI_{M,MI}$  and  $FI_{M,B}$  for all angles.

$$FI_{M,MI} = \max(FI_{M,MI}, FI_{M,B}) \quad (2.26)$$

The implementation of the proposed models in numerical simulations [131] showed that the additional failure modes, interbundle and intrabundle failures, are critical modes under certain stress configurations. The model predicts accurately failure mode and location for transverse matrix related failure initiation, nevertheless, further research is required before using this method for efficient design of NCF composite structures, since the establishment of representative material data is an obstacle for the accuracy of the method.

## 2.6 Gaps in the literature

In the previously presented sections of this chapter, the principal mechanical behaviour of NCF composites and some numerical methods to predict their response was presented. However, the author identified research areas which could be further studied and improvements to the numerical modelling to be made.

The author observed that the majority of the previous studies, are explicitly focused in particular loading scenarios (e.g., only compression or matrix failure) mainly for UD-NCF composites and there is not a recent holistic experimental study for a specific cross-ply NCF system, through which the internal structure, the mechanical properties under the principal in-plane,

interlaminar (Mode I and II), translaminar (Compact Tension) and Low Velocity Impact (LVI) loading scenarios to be examined. Therefore, the author decided to perform an extended and detailed experimental study to provide an integrated characterisation of the Toray T700S [132] - Solvay MTM57 [133] and Toray T700S-Sicomin SR8100 [134] systems fabricated using the RFI and RTM manufacturing methods. Furthermore, the effect of deliberate imperfections in terms of in-plane fibre misalignment but coupled with out-of-plane fibre misalignment is examined in compression.

Concerning the analytical formulation and numerical modelling of cross-ply NCF composites, the author noticed that with regards to the prediction of the longitudinal stiffness, none of the approaches described in subsection 2.5.1, explicitly distinguished the influence of the in- and/or out-of-plane waviness on the longitudinal stiffness reduction, therefore in this work, a novel global knock-down factor is presented, composed by two individual sub-terms accounting the explicit influence of the in- and out-of-plane waviness in the form of angular misalignment.

Additionally, in the previous studies the stitches have not been accounted as a material constituent in terms of volume fraction and their contribution to the mechanical properties at layer level is neglected. In this project, the mechanical properties of the composite are not obtained directly from mechanical testing by homogenising the NCF blankets or applying reverse ROM, but instead, a multiscale approach is presented which is based on the individual mechanical properties of the constituents of the layer, including the mechanical properties and the orientation of the stitches. This approach maintains a predictive character since only a few parameters are necessary to be extracted by the mechanical tests.

The failure criteria presented in subsection 2.5.2, were originally developed for UD laminates and they either completely neglect fibre misalignment or it is only partially included to the failure criterion. Even in LaRC05, a state of the art set of failure criteria, the described fibre misalignment is not clarified whether it refers to in- or out-of-plane misorientation. Also, the calculated angle is an accounting magnitude which practically incorporates all the imperfections of the specimen as obtained through the compression strength after performing a compression test and does not necessarily correspond to an actual fibre misalignment. In this research

project a different approach is followed through which fibre misalignment (in- and out-of-plane) is extracted from the experimental observations and directly input to the failure criteria, without requiring to perform the actual compression test. As a result of this a new failure criterion in fibre compression is proposed and compared against the already performed test results. Moreover, an integrated layerwise set of failure criteria based on Continuum Damage Mechanics (CDM) is presented which also incorporates the enhanced interlaminar fracture toughness of NCF composites, expressed as in-situ strengths which are dominant parameters in matrix dominated failure modes.

As noted in the literature review, the translaminar behaviour of NCF composites is neither thoroughly investigated experimentally nor numerically, as a result of this a series of CT tests is performed. The influence of the particular NCF internal structure is associated with the observed damage mechanisms which were gradually developed during the test. Supplementary to the CT mechanical testing, a computational study is carried out in which a new exponential law, originally inspired by F. Ehrich [135], during element failure is implemented to describe the unloading phase of fibre tension, aiming to accurately capture the load progress and fibre bridging while crack evolves. The unloading phase in other studies is modelled with a cohesive law. The disadvantage of this approach is that it does not adopt a predictive character in terms of the orientation of the crack evolution, since it assumes that the crack evolves along the symmetry plane of the specimen and perpendicular to load direction, while this might not be the case for quasi-isotropic lay-up configurations with excessive matrix damage or for specimens with structural imperfections. A comparative study is performed against the conventional linear unloading path and the prediction of load evolution during crack growth, together with the corresponding R-curves is discussed.

Although the LVI cases for NCF composites have been researched in detail both experimentally and numerically, to the best of author's knowledge, there is not any LVI study to account imperfections of NCF composites to date. Hence, deliberate in-plane fibre misalignment and defects within the laminate in the form of split layers, were introduced in LVI plates and tested for a range of impact energy levels (i.e., 30 to 75J). The scope of this study is to identify

the extent that these imperfections affect the structural integrity, mechanical response, energy absorption, and damage mechanisms of the plates. Moreover, Finite Element analyses are performed and the newly developed constitutive model is validated against the LVI tests.

# Chapter 3

## Experimental procedure and observations

### 3.1 Introduction

The experimental study of this project involves a series of both standard and non-standard mechanical testing so that a pool of experimental data was generated against which the newly proposed failure criteria could be validated. It was decided to perform mechanical testing which includes in-plane failure, interlaminar fracture, failure propagation and impact damage in order to obtain a holistic view of the structural performance of the material system in quasi-static and impact conditions. However, prior to the mechanical testing, it was considered necessary by the author to investigate other parameters of specimens which can potentially affect their mechanical response. Such parameters are the laminate fabrication method where in the current project the RFI and RTM methods were used, the actual volume fraction of the constituents and the specific features of the internal structure of the laminates at mesoscale (layer) level that characterise NCF composites, namely fibre misalignment, voids and defects.

## 3.2 Material Systems

The selected fabric for this study is the cross-ply ( $+45^\circ/-45^\circ$ ) pillar stitched preform of Toray T700 fibres - 12k filament yarn, weighing 300 gsm, evenly distributed between the  $+45^\circ$  and  $-45^\circ$  layers. The stitches are made of polyester of 8 gsm areal mass with stitch length equal to 2.5 mm. The ( $+45^\circ/-45^\circ$ ) biaxial fabric is opted mainly because of its improved fibre alignment, good mechanical performance under extreme shear and torsion and better placement in complex geometries (as in a wing structure) compared to UD NCFs [4]. The NCF was supplied by Cristex Composite Materials. With regards to the resin system, for the RFI process the MTM57 resin film was selected which is a medium temperature, multi-purpose epoxy, supplied by Solvay [133], while for the RTM method the Sicomin SR8100 [134] low viscosity two component epoxy system was opted, together with the SD8824 hardener which is specifically formulated for RTM processes and is characterised by similar mechanical properties with MTM57. In Appendix B.1.1 the mechanical properties of the fibres and the two resin systems are presented (Tab. B.1, B.2, B.3).

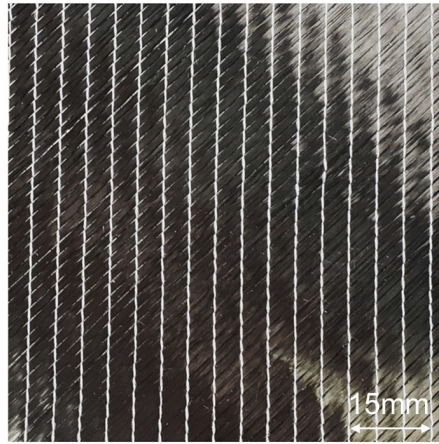


Figure 3.1: *Illustration of the T700 biaxial NCF.*

## 3.3 Laminate Fabrication

Seven laminates were manufactured in total, five with the RFI process and two with RTM. Specifically, the specimens for in-plane (tension, compression and in-plane shear), interlaminar



(DCB and 4pt-ENF) and translaminar (CT) fracture tests are obtained from the RFI laminates, while the LVI panels were cut from the RTM laminates. The target fibre volume fraction,  $V_F$  for each laminate was 60%, however the actual volume fractions were determined both with Thermogravimetric Analysis (TGA) and optical microscopy (OM).

### 3.3.1 Resin Film Infusion

RFI laminates were vacuum bagged to 120 °C at a ramp rate of 100 °C/hour, applied autoclave positive pressure of 7 bar and dwelled for 1 hour before a non-controlled cool down to room temperature, as suggested by Solvay [133]. The MTM57 resin films have areal density of 300 gsm and average thickness of 0.27 mm [133]. The laminates corresponding to tension and compression specimens (RFI\_1, RFI\_2) were formed by a  $[0^\circ/90^\circ]_{4S}$  lay-up resulting in a 2 mm thickness as suggested by the respective test methods (ASTM D3039 [136] and Haeberle & Matthews [137]). Three resin films were placed through-the-thickness at every second blanket of the preform as illustrated in Fig. 3.2a). Out of the third laminate (RFI\_3) the in-plane shear specimens are produced and consisted of a  $[45^\circ/-45^\circ]_{4S}$  lay-up corresponding to a 2 mm thick laminate as defined by ASTM D3518 [138]. Like in RFI\_1, RFI\_2 laminates, three MTM57 films were placed at every second NCF blanket through the thickness of the RFI\_3 dry preform too (Fig. 3.2 a)). The specimens for DCB and 4pt-ENF tests are in accordance with the ASTM D5528 [139] and Davidson & Martin [140], [141] methods respectively and were cut from a  $[90^\circ/0^\circ]_{6S}$  laminate (RFI\_4) of 3.4 mm thickness. Moreover, in laminate RFI\_4, a PTFE film has been inserted at 80 mm in the midplane of the laminate, so that the pre-crack for DCB & 4pt-ENF specimens to be formed. Fig. 3.2b) presents the allocation of six resin films in RFI\_4 preform which also follows the same approach with the previous RFI laminates.

Although in the previous laminates the lay-up and hence their thickness was defined by the respective testing methods, for the fifth laminate out of which the CT specimens will be cut (RFI\_5) the procedure is more complex. The thickness of this laminate is dependant on the selected specimen's geometry and is determined by the ability of the specimen to withstand premature compressive failure at its rear end. Therefore, a numerical eigenvalue buckling

analysis is required to estimate the optimal thickness of the specimen as suggested by Katafiasz & Iannucci [142]. A detailed description of the eigenvalue analysis is presented in Chapter 4 of this thesis. For the scope of this subsection only the thickness and lay-up of RFI\_5 is stated which are 7.8 mm and  $[90^\circ/0^\circ]_{14S}$  respectively. Eleven MTM57 films were placed in total in the RFI\_5 NCF preform (Fig. 3.2 c)). Until the 17th NCF blanket, counting from top to bottom, the resin films were allocated at every second blanket, whereas from the 17th blanket to the bottom blanket (28th) the resin films were inserted at every third NCF blanket. The reason for following this approach was based on author's observations in trial laminates with thickness over 6 mm that an even distribution of resin films throughout the thickness of the preform results in higher resin concentrations to the bottom layers and lower to the top. Hence, to alleviate this phenomenon, the author judged that the resin films should be allocated at a coarser pitch, namely every three NCF blankets, for the NCF blankets towards the bottom of the preform.

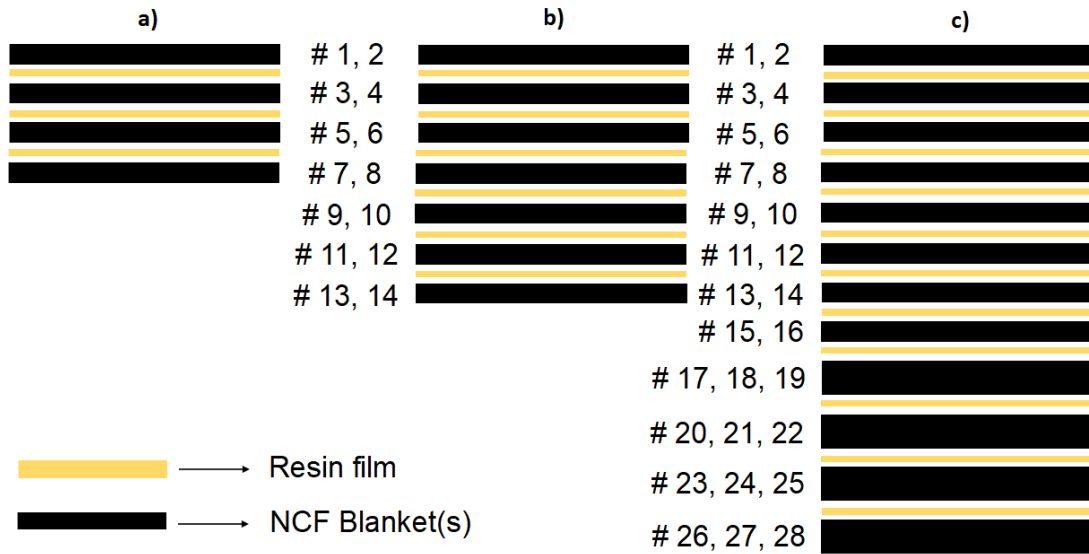


Figure 3.2: Illustration of the positioning of the MTM57 resin films in a) RFI\_1, RFI\_2, RFI\_3, b) RFI\_4 and c) RFI\_5 panels. Not scaled for reference only.

### 3.3.2 Resin Transfer Mould

The RTM process, for the two laminates (RTM\_1 and RTM\_2), was conducted in a closed mould tool (Fig. 3.3) supplied by Composite Integration Ltd with maximum pressure capacity of 6 bar, able to fabricate up to 500 mm x 500 mm laminates. The NCF preform was cut at a

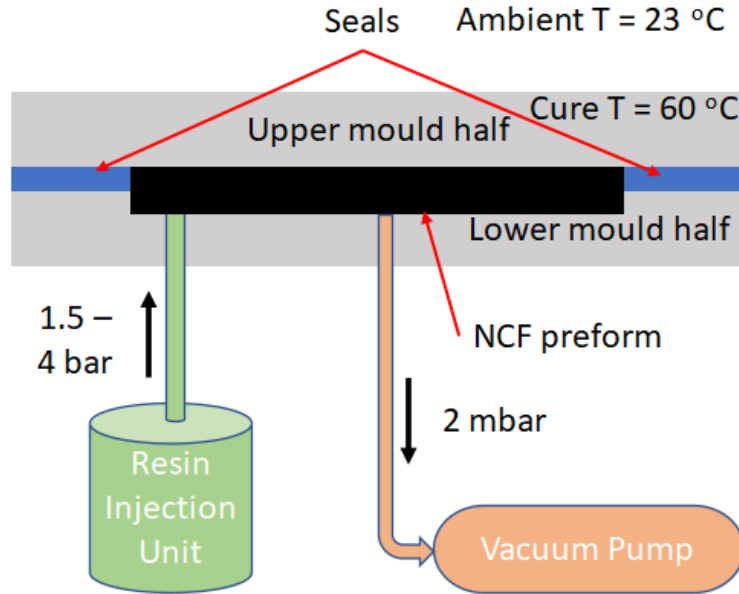
smaller size, 485 mm x 485 mm to ensure that it will not overlap the sealing of the mould. A 5 mm cavity was adjusted corresponding to a layer sequence  $[(-45^\circ/45^\circ) (0^\circ/90^\circ)_3 (-45^\circ/45^\circ) (0^\circ/90^\circ)_3]_S$  as defined for LVI plates in ASTM D7136 [143]. The preform was then placed on the lower mould before the resin is injected. Intentionally, it has been decided not to preheat the mould, since during previous trial laminate fabrications for  $V_F = 60\%$ , the author noticed early crystallisation of the resin system in several areas of the cavity affecting the permeability and the uniform spreading of the resin onto the preform. Once the mould tool was tightly clamped, vacuum assistance of 2 mbar was applied to the central valve of the tool for 30 mins to remove the air content within the preform and it was maintained during injection.



Figure 3.3: *Illustration of the RTM closed mould.*

The injection of the resin/hardener mixture to the mould tool was performed with the MKV Hypaject MK III equipment. The resin was mixed with hardener by volume ratio 100/28 as recommended by Sicomin [134], and then loaded to the homogeniser for degassing for approximately 10 mins at room temperature (i.e., 23 °C). An initial injection pressure of 1.5 bar

was applied to avoid movement of NCF blankets [144] and was gradually increased to 4 bar which was the maximum pressure limit for the injection unit. The resin injection process required approximately 10 mins. After the resin pouring process, the mould tool remained closed and mould temperature was regulated to 60 °C for 16 hours to cure the resin. A schematic representation of the RTM system and procedure is shown in Fig. 3.4 .



**NOTE: RTM unit diagram, for reference only.**

Figure 3.4: *Schematic representation of the RTM procedure with vacuum assistance.*

During the entire procedure, mould temperature was monitored by four K-Type thermocouples installed in different regions at the bottom half of the mould. The positions of the thermocouples can be seen in the 3D model of Fig. A.1 in Appendix A. Table 3.1 summarises the fabricated laminates, their lay-up, dimensions, thickness and the corresponding tests.

### 3.4 Laminate Inspection

The ultrasound C-scanning method was implemented as a Non-Destructive Technique (NDT) to evaluate the quality of the panels and select the areas where the specimens will be cut from. The equipment used to scan the laminates is a Triton 1700TT system, with one transmitter and one receiver transducer using a 10 MHz frequency piezoelectric probe. The scanning speed

| Laminate | Lay-up                                                                                    | Dimensions      | Thickness          | Test Specimens  |
|----------|-------------------------------------------------------------------------------------------|-----------------|--------------------|-----------------|
| RFI_1    | $[0^\circ/90^\circ]_{4S}$                                                                 | 380 mm x 380 mm | 2.0 mm $\pm$ 0.06  | Compression     |
| RFI_2    | $[0^\circ/90^\circ]_{4S}$                                                                 | 310 mm x 335 mm | 2.18 mm $\pm$ 0.04 | Tension         |
| RFI_3    | $[45^\circ/-45^\circ]_{4S}$                                                               | 390 mm x 300 mm | 2.19 mm $\pm$ 0.05 | In-plane Shear  |
| RFI_4    | $[90^\circ/0^\circ]_{7S}$                                                                 | 360 mm x 370 mm | 3.5 mm $\pm$ 0.1   | DCB & 4pt-ENF   |
| RFI_5    | $[90^\circ/0^\circ]_{14S}$                                                                | 270 x 270 mm    | 7.8 mm $\pm$ 0.15  | Compact Tension |
| RTM_1    | $[(-45^\circ/45^\circ) (0^\circ/90^\circ)_3 (-45^\circ/45^\circ) (0^\circ/90^\circ)_3]_S$ | 485 mm x 485 mm | 4.95 mm $\pm$ 0.15 | LVI             |
| RTM_2    | $[(-45^\circ/45^\circ) (0^\circ/90^\circ)_3 (-45^\circ/45^\circ) (0^\circ/90^\circ)_3]_S$ | 485 mm x 485 mm | 5.0 mm $\pm$ 0.1   | LVI             |

Table 3.1: *Fabricated laminates with RFI and RTM methods.*

was set at 80 mm/min. Ultrasonic C-scans did not indicate any major dry areas or potential voids, inclusions and delaminations both for the RFI and RTM laminates. In Figs. 3.6a), b), c), d) and e) and 3.6 the fabric architecture for all laminates is visible without defects. In Fig. 3.5d) the areas where the PTFE film inserts are situated are explicitly pointed.

### 3.5 Fibre Volume Fraction Calculation

The constituent fractions of the laminates were determined using the TGA burn-off method (ASTM D 3171-99 [145]) as well as the analytical microscopic approach. As constituents are defined the following: the fibres (T700), the resin system (MTM57 and SR8100) and the polyester stitches. The degradation temperatures significantly varied between the constituents, as a result of this, each constituent could be burnt off individually and their weight fraction could be obtained without any influence from the other constituents. During the TGA method, each composite sample is heated and the sample weight is recorded during constant ramp rates to elevated temperatures. Then, these data are used to calculate composite's volume fraction for each of their constituents. Since the current NCF preform has stitch pitch equal to 2.5 mm, the composite samples were cut in a size of 3 mm x 3 mm in order to be as representative as possible of a unit cell and to comply with the TGA limitation to accommodate larger samples. The densities of the carbon fibre, polyester stitches and resin were measured individually with the use of the AccuPyc II 1340 pycnometer since they are required for the weight to volume

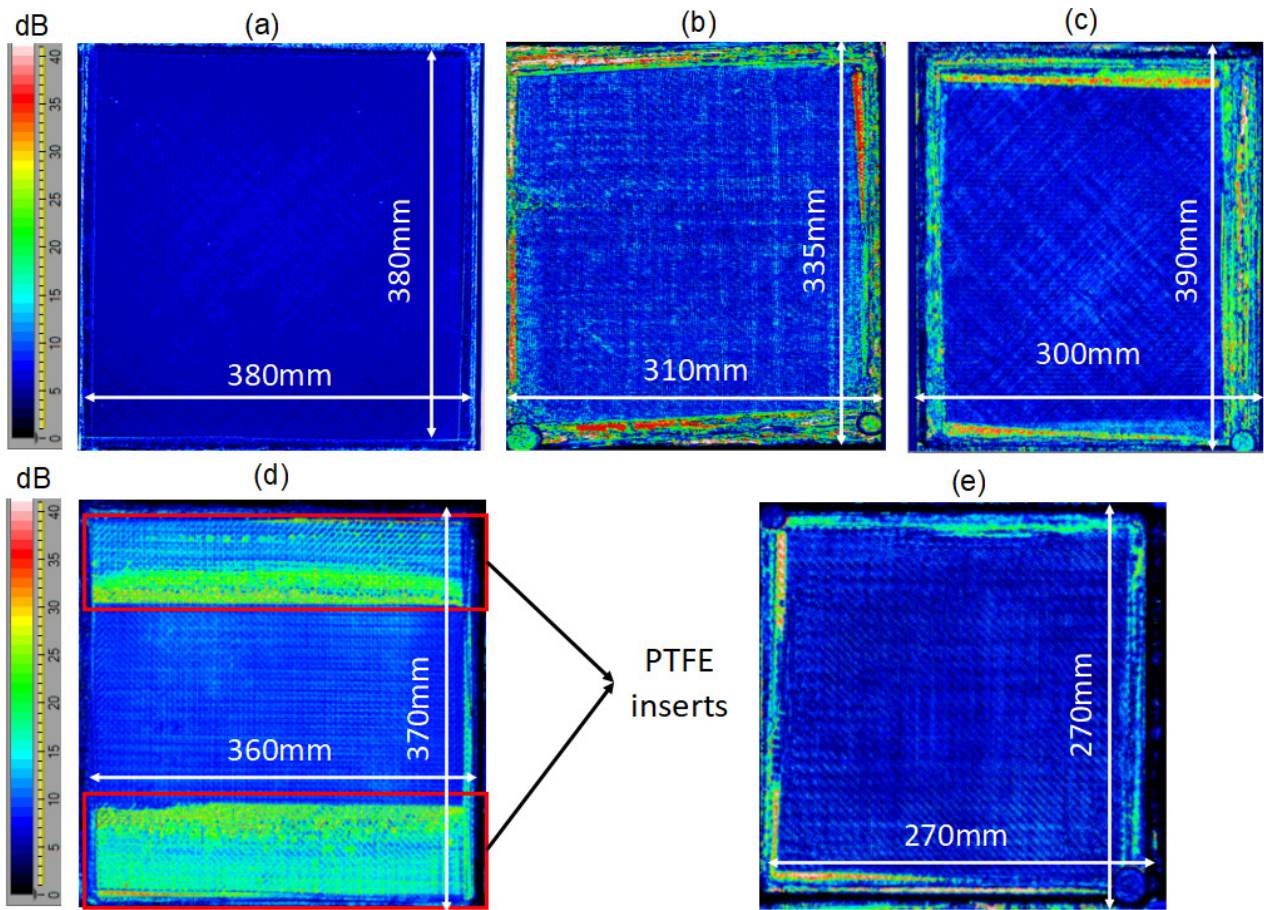


Figure 3.5: C-Scan images of the RFI panels a) RFI\_1, b) RFI\_2, c) RFI\_3, d) RFI\_4 and e) RFI\_5.

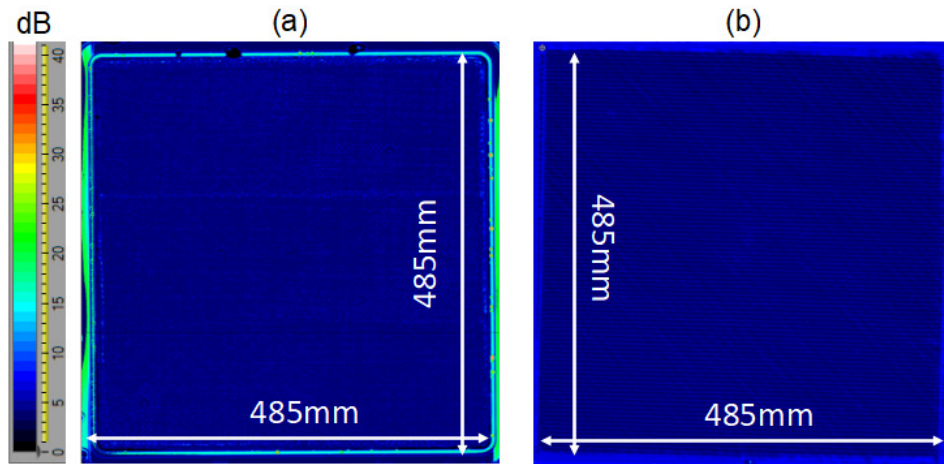


Figure 3.6: C-Scan images of the RTM panels a) RTM\_1 and b) RTM\_2.

calculation. Prior to the density measurement, the samples of fibres, stitches and the two resin systems were placed for 48 hours into an oven at 40 °C with air extraction, so that possible moisture to be removed. Fig. 3.7 presents the measured densities of the constituents.



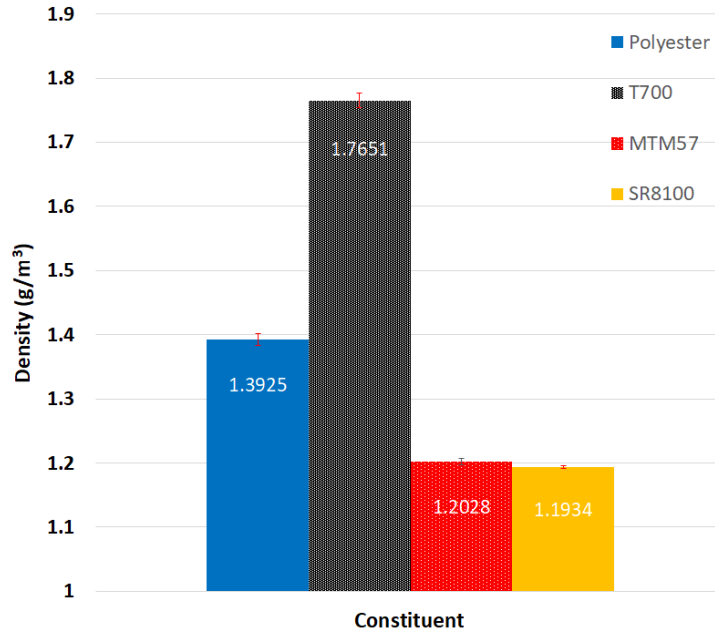


Figure 3.7: *Density chart of NCF composite constituents: Polyester stitches, carbon fibres T700, epoxy/resin MTM57 and epoxy/resin SR8100.*

The volume fraction calculation approach was based on the work published in [146] and [147]:

$$W_o = W_R + W_C + W_S \quad (3.1)$$

where,  $W_o$  is the dry mass of the sample in (g),  $W_R$  the mass of the resin in (g),  $W_C$  the mass of the carbon in (g) and  $W_S$  the mass of the stitches in (g).

$$W_{T1} = W_C + W_S \quad (3.2)$$

where,  $W_{T1}$  is the mass of the sample after the first temperature exposure in (g).

$$W_{T2} = W_C \quad (3.3)$$

where,  $W_{T2}$  is the mass of the sample after the second temperature exposure in (g). The volume fraction of 'x' constituent is obtained through the following relationship:

$$v_x = \frac{W_x}{\rho_x \cdot V} \quad (3.4)$$

where,  $\rho_x$  is the density of constituent 'x' in (g/cm<sup>3</sup>),  $W_x$  is the mass of constituent 'x' (g) and  $V$  is the total volume of the sample (cm<sup>3</sup>), determined by the division of sample's mass over its density. The density of the sample is obtained from the pycnometer and its mass from a precision scale. During burn-off tests it has been opted to use nitrogen gas instead of air as mean of combustion. It has been reported [147] that air can result to oxidisation of fibres and thus unstable burn-off, whereas nitrogen does not interact with the fibres. Fig. 3.8 presents the burn-off chart of polyester stitches, whose burn-off temperature has been identified to be at 600 °C. Respectively, the burn-off temperature of the resin/epoxy system (MTM57) was obtained from Tomas Katafiasz's thesis [148] and equals to 370 °C, while the same temperature was found for the SR8100. Figs. 3.9 to 3.13 present the weight percentage fractions of each RFI laminate during the TGA analyses, whereas Figs. 3.14 and 3.15 show the same parameters for the two RTM laminates. As it can be observed from the above-mentioned figures, the burn-off planning consisted of 4 stages. The first stage involved a temperature increase up to 370 °C with ramp rate of 50 °C/min. During this stage any possible moisture is removed. The following stage is isothermal at 370 °C for 400 minutes. At this stage the resin/epoxy burn-off takes place. Stage 3 corresponds to the raise of temperature up to 600 °C at ramp rate of 10 °C/min and is then followed up by an 120 minutes isothermal stage, where the polyester stitches are burned-off.

Fig. 3.16 presents the volume fraction results of each constituent for all laminates in the form of bar chart. The weight percentage was considered to have plateaued once the mass rate change reached the value of 0.1 %/min. It can be seen that there is limited scatter in all seven laminates, however it has generally been noted that the size of the sample needs to be as close as possible to the size of the unit cell, otherwise the possibility to observe variations of the volume fractions is high. Furthermore, the volume fraction results of RFI laminates demonstrate that fibre volume fraction is in principle higher than the target fibre volume fraction (60%) and significantly higher than the RTM laminates. This can be attributed to two reasons. The first reason regards the fact that some areas of the resin films were dry of resin



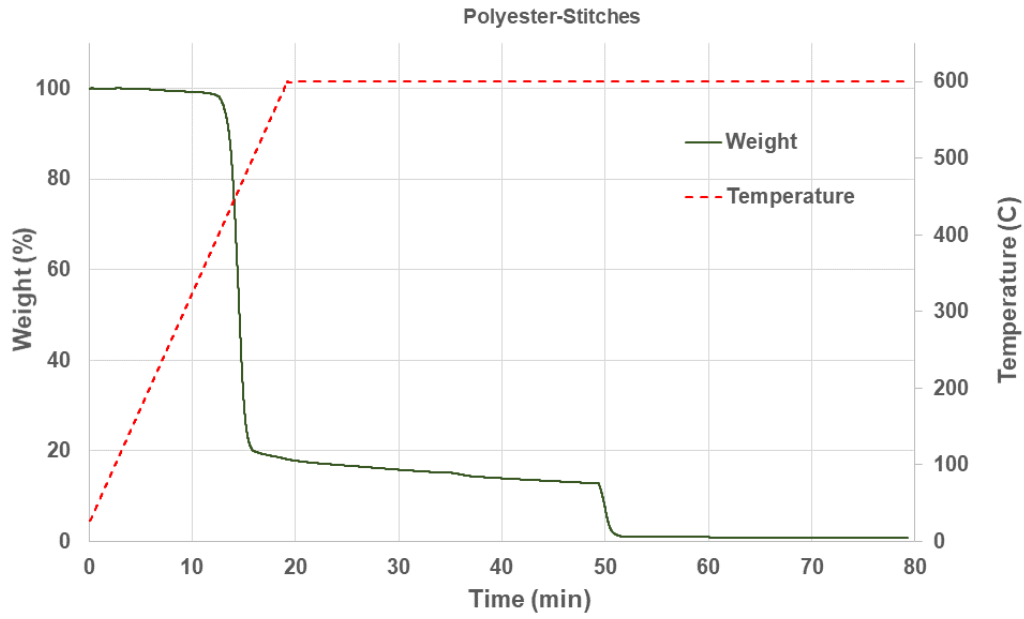


Figure 3.8: *Polyester stitches burn-off, weight %.*

which results in smaller quantity of resin into the laminate. Secondly, unlike the RTM process, the RFI manufacturing process has the disadvantage that the exact thickness of the laminate cannot be controlled, since there can only be integer number of blankets and resin films used, resulting in scatter of the volume fractions.

A second method to evaluate the volume fractions has been employed using the image processing software Fiji/ImageJ (ver. 1.52n) [149]. Images of the cross-sections of the laminates were obtained from the optical microscopy and analysed by the software. The rectangular cured samples were cut from the laminates and potted. After the grinding and polishing of the samples, an automatic zoom Zeiss camera was used to capture their polished cross sections. The microscopic images were imported to the image processing software and converted into an 8-bit binary format. A threshold analysis of the images was considered necessary so that the pixels associated with carbon fibres to be numerically identified. The lighter hue corresponded to the fibres whereas darker scales to resin. As a result of this the carbon fibres were set to a white format and the other composite constituents were set to black. The same approach was followed for the evaluation of the polyester stitches volume fraction. Fig. 3.17 shows the previously described conversion from raw microscopic image into the binary threshold for laminate RFI\_1, while Fig. 3.18 for RTM\_1. The pixels were then counted according to their

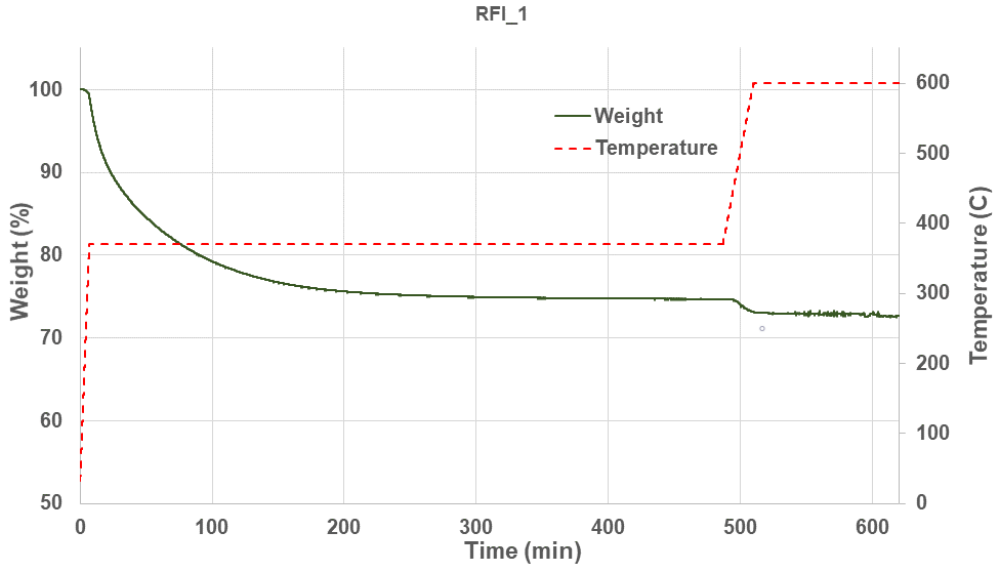


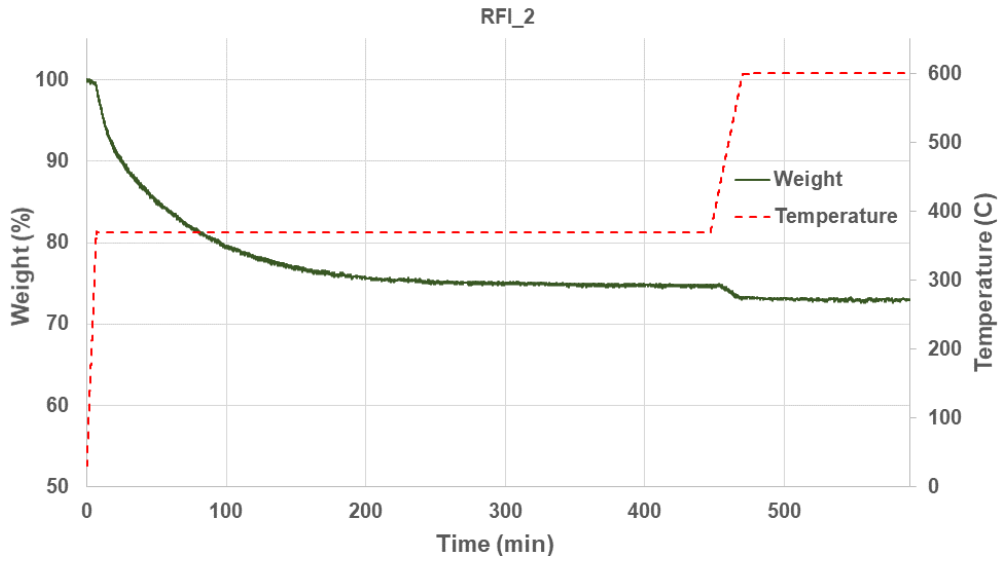
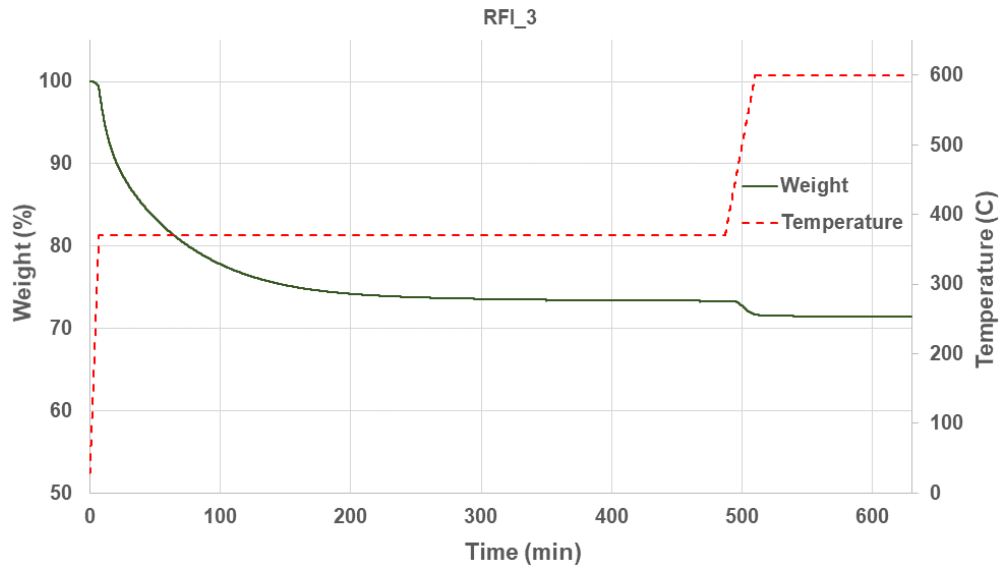
Figure 3.9: *RFI\_1* burn-off, weight %.

binary identifier and an area percentage was determined. Similarly, the content of polyester stitches are presented in Fig. 3.19.

The advantage of the optical microscopy approach compared to the TGA is that the fibre volume fraction within the fibre bundles, ( $V_{Fb}$ ) can be obtained (Fig. 3.20). That is significant for modelling Representative Volume Elements at fibre bundle level. From the analytical microscopy the obtained average values of  $V_{Fb}$  are presented in Table 3.2, where also the values of volume fractions from all panels are summarised.  $V_R$  is the volume fraction of the resin and  $V_{pes}$  the volume fraction of the polyester stitches. The average volume fraction of fibres in a layer, ( $V_F$ ) is defined by:

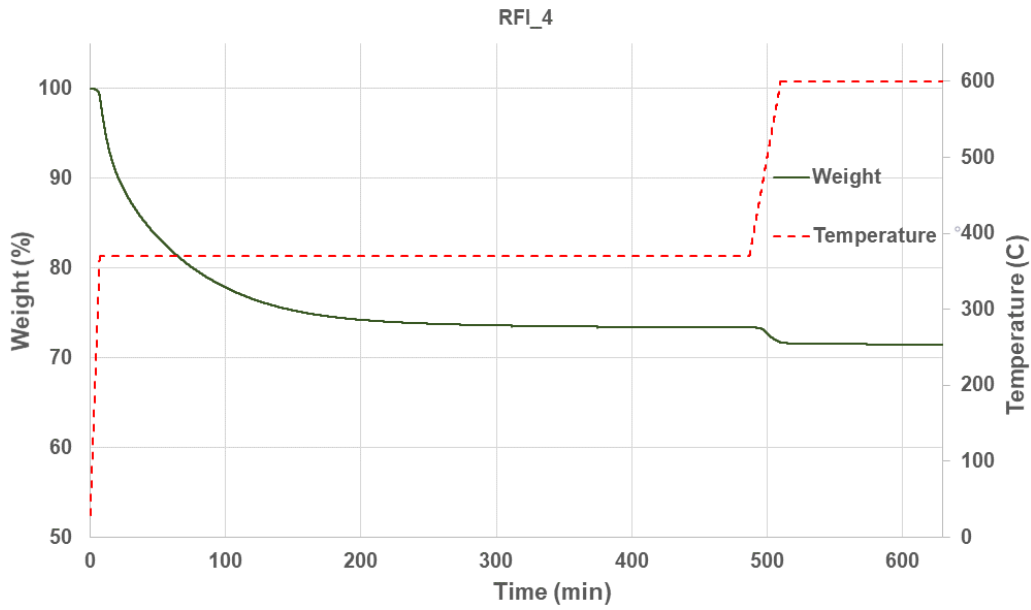
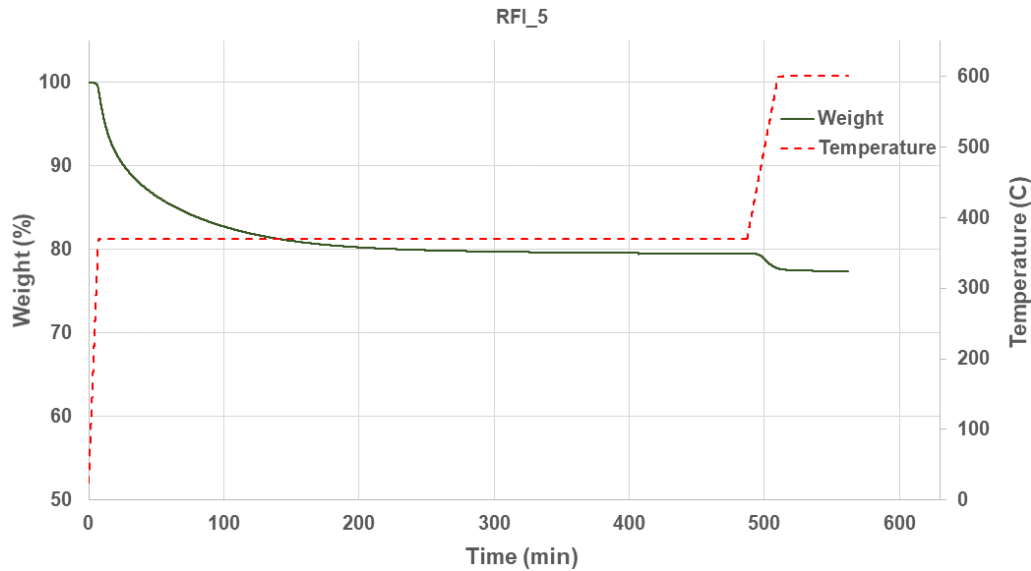
$$V_F = V_{Fb} \cdot V_b \quad (3.5)$$

where,  $V_b$  is the volume fraction of the bundles in the layer. Comparing the volume fractions between TGA and optical microscope, it can be observed that the deviation of the values is within the acceptable limits. However, considering the labour time which is longer in the optical microscope, it has been decided by the author to use only the TGA method for future volume fraction calculations.

Figure 3.10: *RFI\_2* burn-off, weight %.Figure 3.11: *RFI\_3* burn-off, weight %.

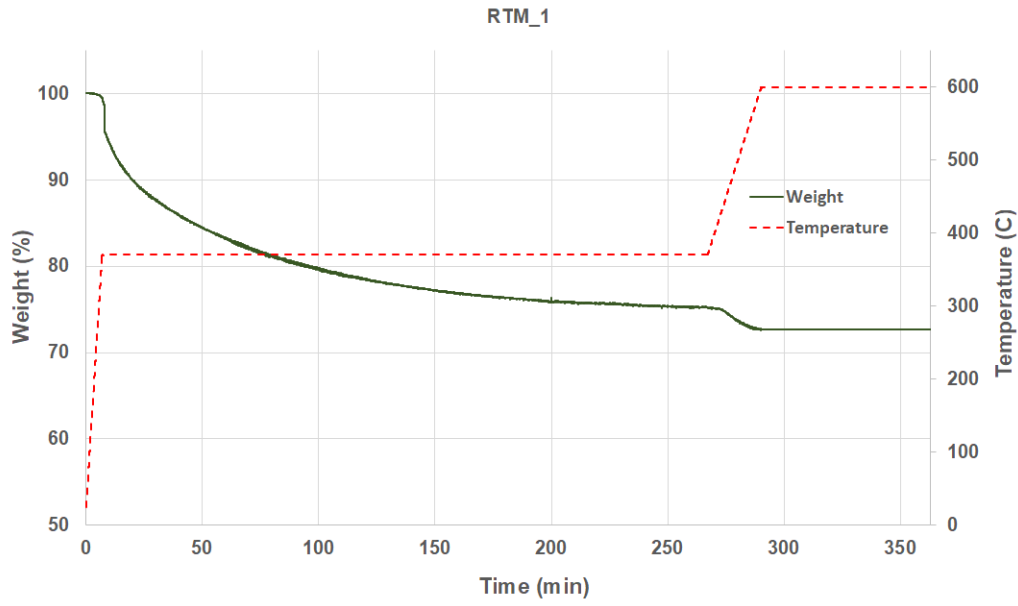
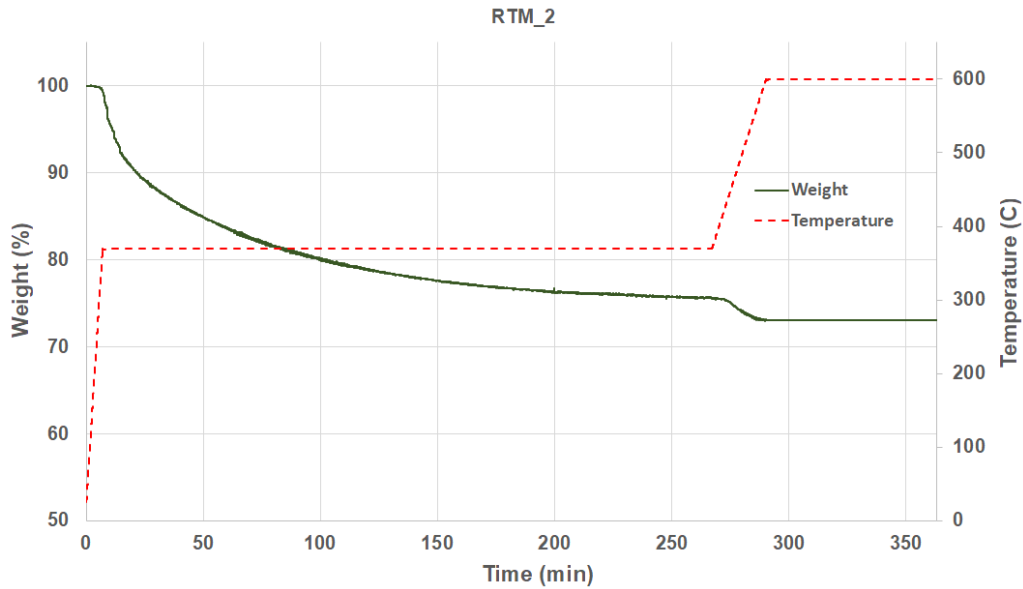
### 3.6 Mesoscale Structure Observation

Particular focus was given to the internal structure of NCF composites and especially to the identification of possible fibre waviness within the laminates. Several researchers as for example [5, 22, 26] have extensively reported the influence of fibre waviness and resin pockets in leading to low compressive strengths of NCF composites. The present section aims to present the findings of the mesoscale structural level investigations for all fabricated laminates.

Figure 3.12: *RFI\_4* burn-off, weight %.Figure 3.13: *RFI\_5* burn-off, weight %.

### 3.6.1 Fibre Waviness

In cross-ply composites the  $0^\circ$  bundle are nesting into the matrix channels between each two adjacent  $90^\circ$  bundles. Since the matrix regions between the  $90^\circ$  bundles are periodically distributed, the waviness of the  $0^\circ$  bundles can be assumed as a periodic wave [26], expressed with wavelength and amplitude. However, in the event the waviness is not periodic, other parameters than the wavelength should be introduced, such as the maximum inclination angle. As it can be seen in Fig. 3.21 in cross-ply NCF composites the stitching thread induces waviness,

Figure 3.14: *RTM\_1* burn-off, weight %.Figure 3.15: *RTM\_2* burn-off, weight %.

in the sense that the thread separates the  $0^\circ$  bundle from the adjacent bundles in the area closest to the thread. As a result of this, it is highly possible to observe fibre waviness in- and out-of plane. This phenomenon is more obvious when the fabric is produced with high stitch tension; however, there are cases where the stitches induce only minimum fibre waviness. In the top right Fig. 3.21 is clear that the effect of stitching is insignificant, therefore it is currently difficult to establish a direct correlation between the presence of the stitches and the induced fibre waviness.

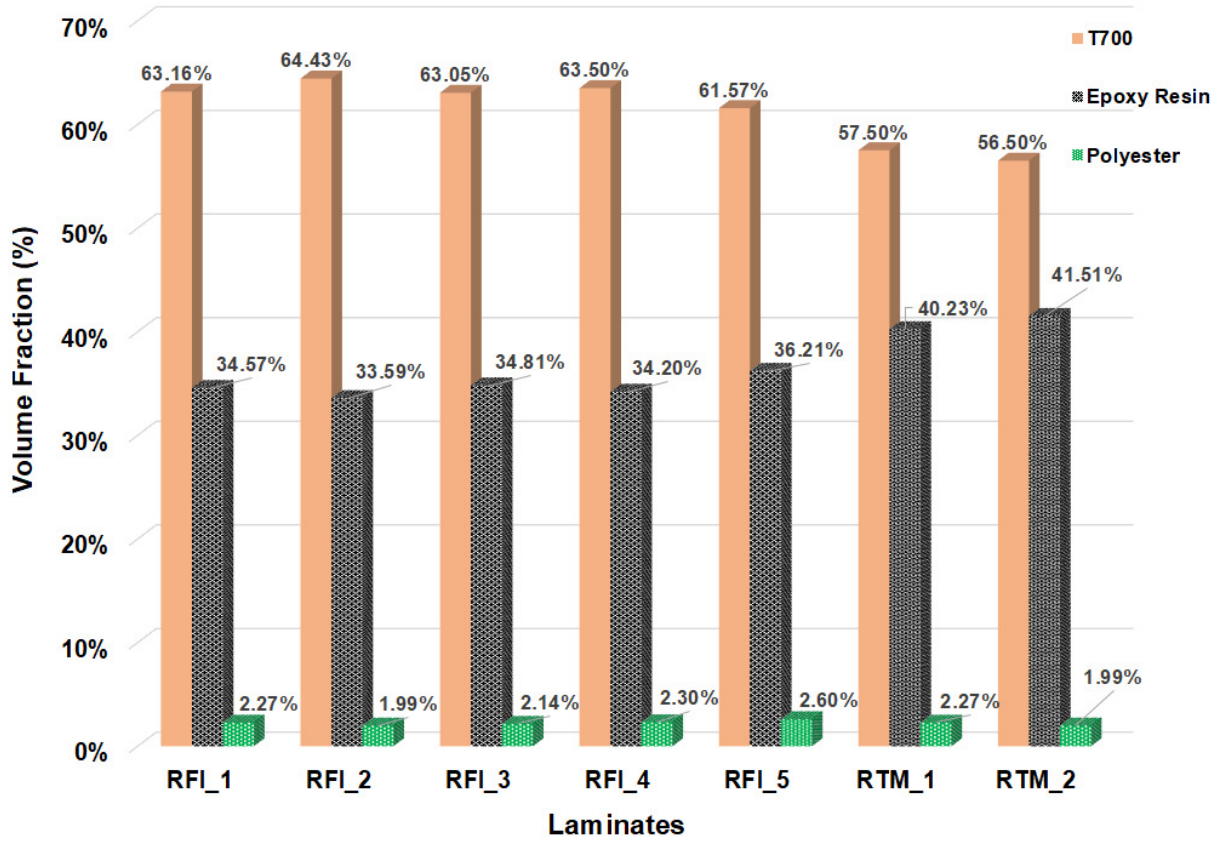


Figure 3.16: Summarised burn-off test results expressed as volume fractions for the RFI and RTM laminates.

The fibre waviness in terms of fibre misalignment angles are directly related to the stiffness and strength reduction. Gutkin et al. [54] presented with the use of FE models that under compression, an initial out-of-plane fibre misalignment angle less than  $2^\circ$  caused fibre shear failure, in parallel, it was shown that as fibre misalignment angle approaches zero, i.e. assuming perfectly straight fibre, the strength of the composite increases exponentially, revealing that the sensitivity on compressive strength to misalignment angle is very high.

### 3.6.2 Measurements of Waviness in Cross-Ply Laminates

In this study primarily OM has been used for obtaining the in- and out-of-plane waviness of fibre bundles. The in-plane fibre waviness due to binding yarns is formed in a diamond shape (Fig. 3.22 - top) and was obtained from the top and bottom surfaces of each laminate. Edgren et al. [24] suggested to use the measured mean values of the stitch gauge, namely the distance between two adjacent stitch lines for the out-of-plane waviness wavelength, however no direct

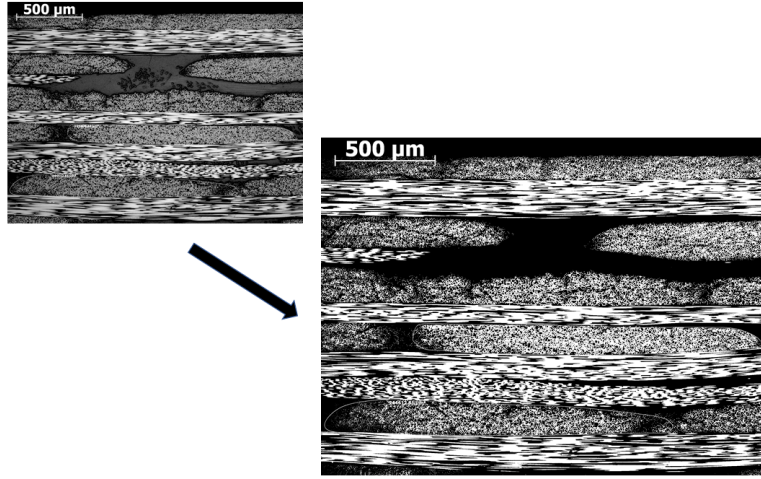


Figure 3.17: *Microscopic through-the-thickness unit cell of laminate RFI\_1, Fiji/ImageJ binary threshold processing carbon fibres.*

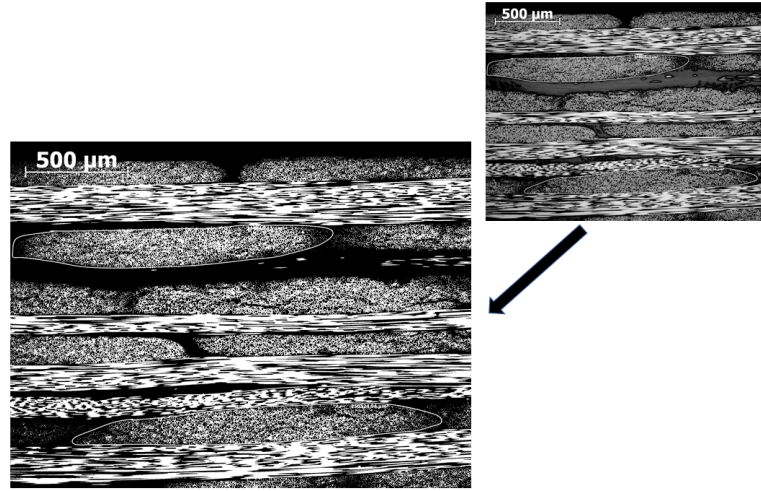


Figure 3.18: *Microscopic through-the-thickness unit cell of laminate RTM\_1, Fiji/ImageJ binary threshold processing carbon fibres.*

correlation between stitch gauge length and out-of-plane fibre misalignment was identified in the present study. But, since it was difficult to observe a periodic pattern of out-of-plane fibre waviness, the fibre misalignment was described in terms of angles rather than waves. In Fig. 3.22 a comparison is illustrated between the wave properties (amplitude,  $A$  and wavelength  $\lambda$ ) and the angular interpretation of fibre misorientation for in- (top) and out-of-plane fibre misalignment (bottom).

In recent studies [28,29,150], advanced image analysis techniques were developed for measuring accurately the in- or out-of-plane fibre waviness, and are implemented in the present work as well. In Fig. 3.23, the procedure that was followed is presented. Every specimen was partitioned



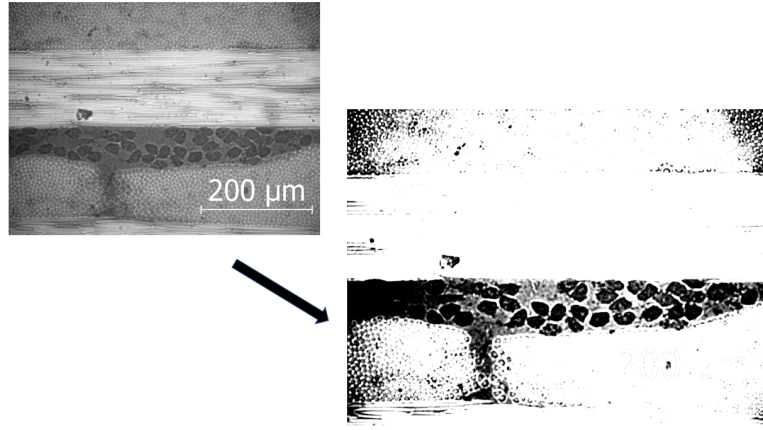


Figure 3.19: *Microscopic through-the-thickness unit cell of laminate RFI\_2, Fiji/ImageJ binary threshold processing polyester stitches.*

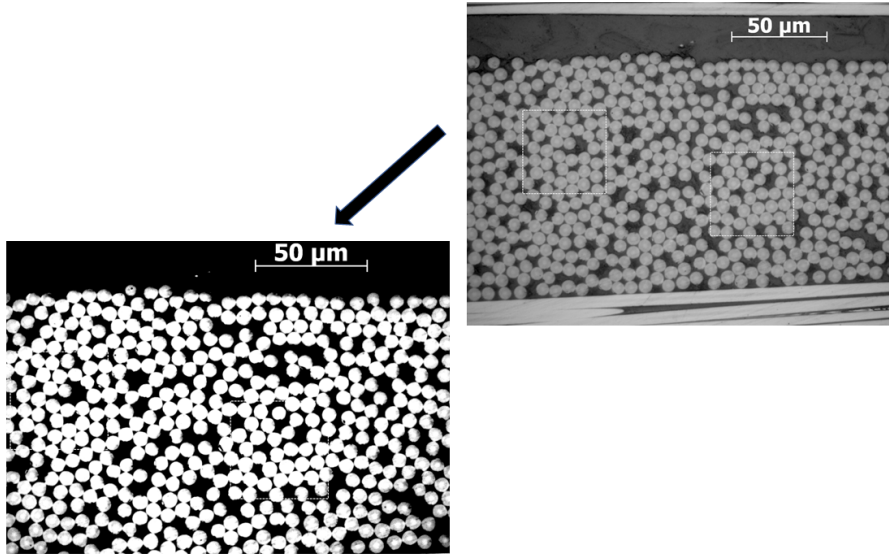


Figure 3.20: *Microscopic view of the fibre bundle of laminate RTM\_2, Fiji/ImageJ binary threshold processing carbon fibres within fibre bundle.*

to sections in the area of the gauge length at a size varying from 20 to 50 mm depending on specimen's dimensions. The OM greyscale images were converted to binary images with the assistance of Matlab (Fig. 3.22). Each section was divided to smaller cells, in order to discretize the curved fibres to straight lines and thus measure the angular misalignment. A spatial resolution of 50 pixels x 50 pixels is determined per cell, while three cells per layer thickness (0.135mm) are assigned. Then with the use of Fast Fourier Transformation (*fft2*) the slopes of the fibres are obtained [29]. For each laminate a mean initial fibre misalignment angle,  $\overline{\theta_I}$  for out-of-plane and  $\overline{\phi_I}$  for in-plane, is calculated as:  $\overline{\theta_I}, \overline{\phi_I} = \frac{1}{n} \cdot \sum_i^n |\overline{\theta_I}, \overline{\phi_I}|$  where,  $n$  is the number of measured angles per laminate. The misalignment angles were determined for



| Laminate | $V_{Fb}$ (%) | $V_b$ (%) | $V_{Fl}$ (%) | $V_R$ (%) | $V_{pes}$ (%) |
|----------|--------------|-----------|--------------|-----------|---------------|
| RFL_1    | 82.0         | 76.3      | 62.5         | 35.12     | 2.35          |
| RFL_2    | 82.9         | 77.0      | 63.8         | 34.2      | 2.0           |
| RFL_3    | 82.7         | 75.5      | 62.4         | 35.35     | 2.25          |
| RFL_4    | 82.5         | 74.0      | 61.0         | 34.6      | 2.4           |
| RFL_5    | 81.7         | 74.7      | 61.0         | 36.75     | 2.5           |
| RTM_1    | 79.0         | 71.6      | 56.5         | 41.51     | 2.4           |
| RTM_2    | 79.9         | 72.0      | 57.5         | 40.23     | 2.3           |

Table 3.2: Volume fractions of laminates, obtained from optical microscope.

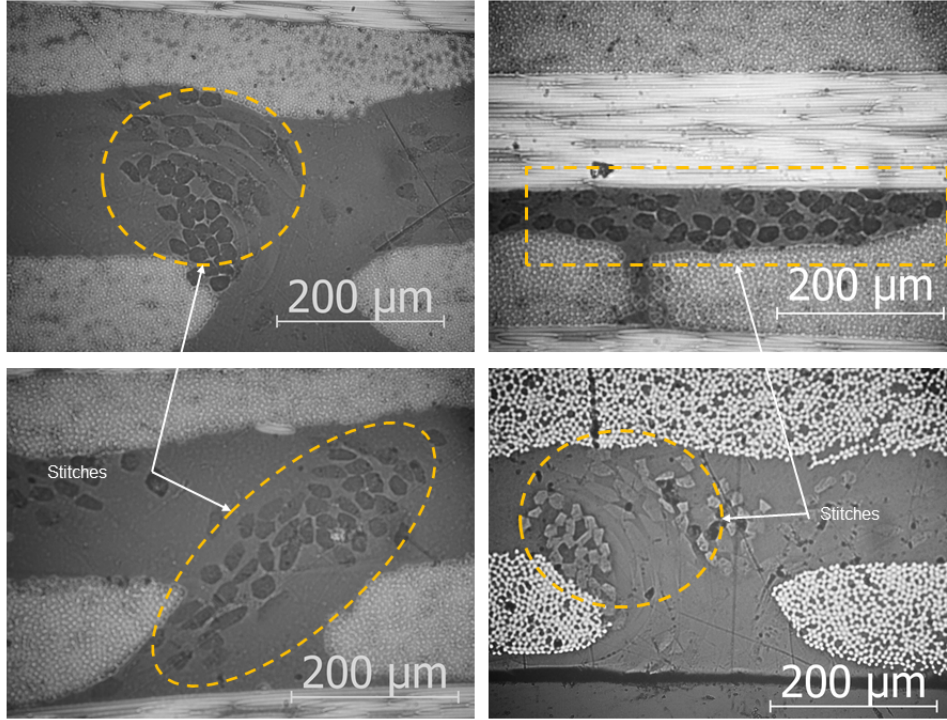


Figure 3.21: Presentation of stitches between the fibre bundles, resulting in out-of-plane fibre waviness. Pictures obtained using OM, panel RFL\_4 (top) and RTM\_1 (bottom).

all individual layers per laminate. In total, 6000 measurements were obtained to define the in- and out-of plane fibre misalignment for all laminates.

In Figs. 3.24 and 3.25 a statistical distribution of the recorded misalignment angles -  $\phi_I$  and  $\theta_I$  of the RFI and RTM laminates is presented. For the RFI panels (Fig. 3.24), the distribution of the recorded misalignment angles -  $\phi_I$  and  $\theta_I$ - is best described by two Gaussian fitting curves with mean value ( $\bar{x}$ )  $2.02^\circ$  and standard deviation ( $\sigma$ )  $1.02^\circ$  for  $\phi_I$  and  $\bar{x} = 3.88^\circ$ ,  $\sigma = 2.28^\circ$  for  $\theta_I$  respectively. The bar chart reveals that the scatter of  $\phi_I$  is much narrower compared to  $\theta_I$ . This can be attributed to the essential dependence of the in-plane misalignment to the

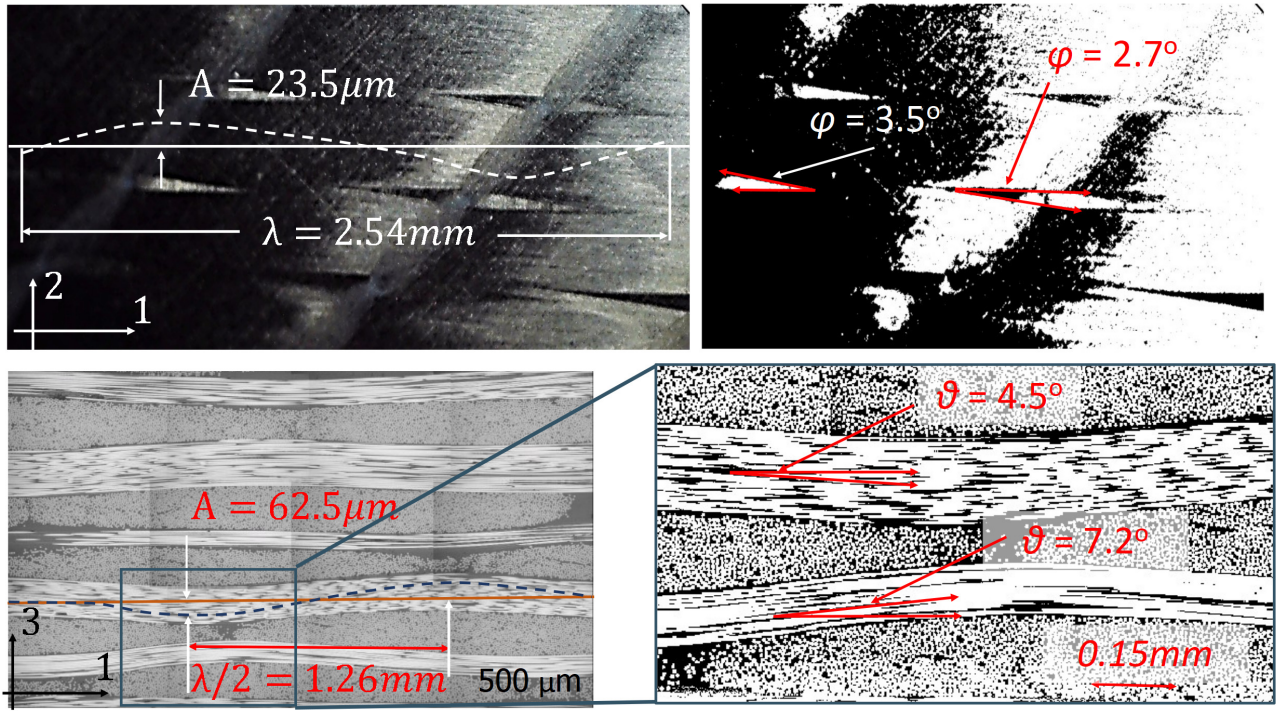


Figure 3.22: In-plane (top, scale: 12:1) and out-of-plane (bottom, scale: 20:1) fibre misalignment binary conversion. Representation of fibre undulation both as a wave and angular misalignment.

stitching parameters such as the pitch length and spacing, which define some periodicity in the misalignment. Similarly, it can be identified that the in-plane misalignment is significantly lower than the out-of-plane due to the geometric constraints that the stitching reinforcement applies to the in-plane direction.

Similar statistical characteristics were also observed for the RTM laminates. Again the recorded misalignment angles are normally distributed with mean value ( $\bar{x}$ ) = 2.1° and standard deviation ( $\sigma$ ) = 0.9° for  $\phi_I$  and  $\bar{x}$  = 3.1°,  $\sigma$  = 1.8° for  $\theta_I$  respectively. Whilst the mean value,  $\phi_I$  of the RTM laminates has very small discrepancy when compared against the respective value of RFI panels, the mean value of  $\theta_I$  for RTM laminates is lower than in RFI laminates by approximately 25%. Although there is no obvious reason that this difference could be attributed to, the author identifies as a possible cause, the fact that the RFI process is more susceptible to errors during lay-up since the resin film inserts in-between the blankets are sticky and thus do not allow corrections when a blanket is misplaced or when wrinkles are formed, while that is not the case for the RTM method, where the operator can correct any observed imperfections during lay-up. Another reason that could possibly contribute to this difference is that in the RTM

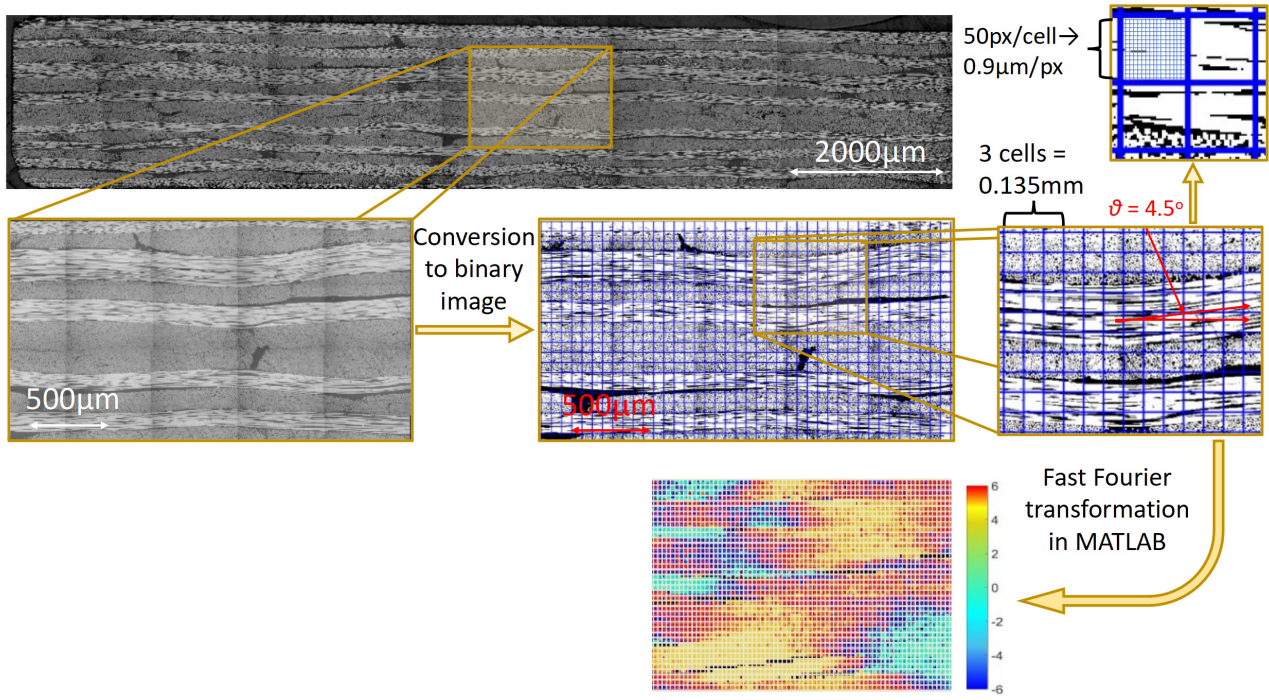


Figure 3.23: Image analysis procedure for out-of-plane misalignment characterisation.

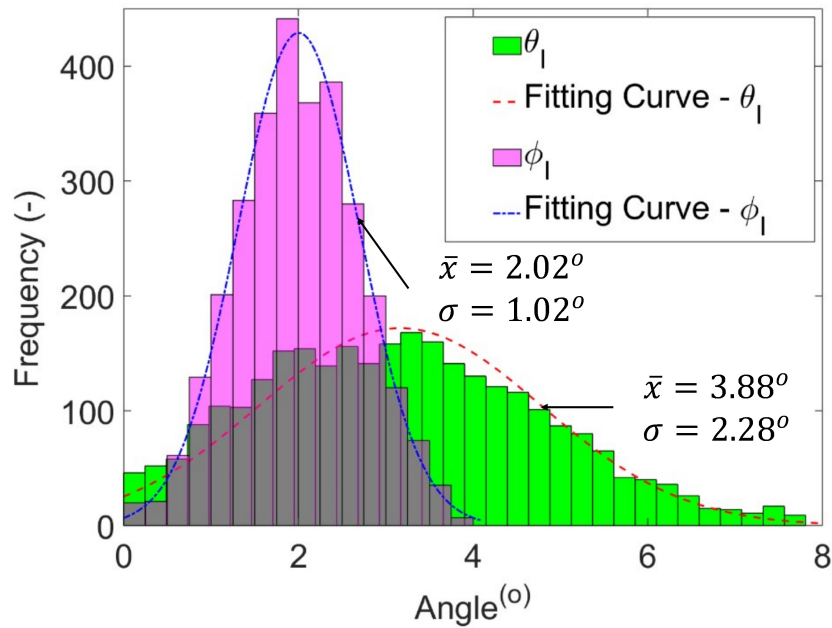


Figure 3.24: Distribution of in-,  $\phi_I$  and out-of-plane,  $\theta_I$  angular misalignments in RFI laminates.

process the NCF preform is tightly squeezed within a mould and therefore major out-of-plane misorientations are limited, whilst in the RFI method the preform is situated inside an autoclave with regulated pressure (i.e., 7 bar in this project).

Table 3.3 summarises the mean and maximum in- and out-of-plane initial misalignment for each



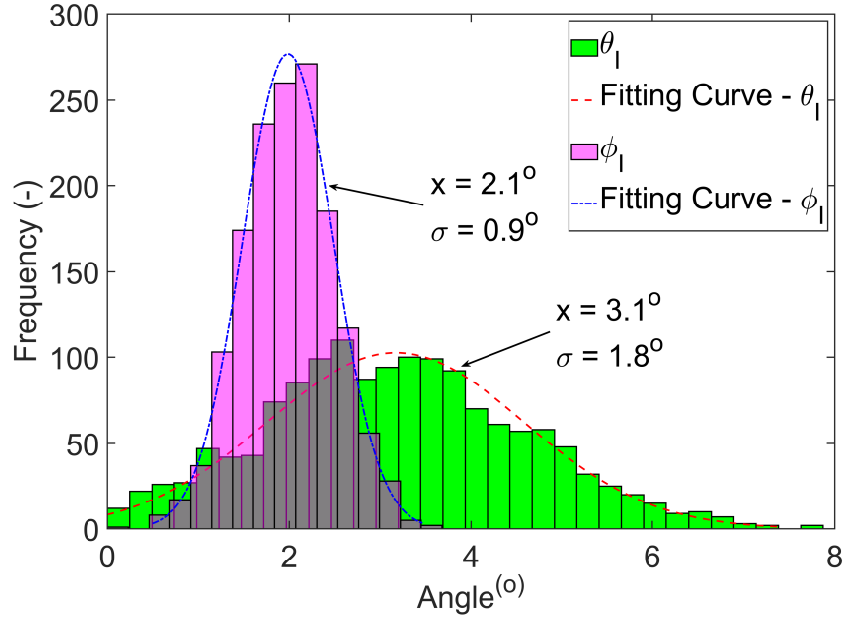


Figure 3.25: Distribution of in-,  $\phi_I$  and out-of-plane,  $\theta_I$  angular misalignments in RTM laminates.

laminate. It can be identified that laminates RFL1, RFL2 and RFL3 exhibit high out-of-plane fibre misalignment ( $\bar{\theta}_I \approx 4.0^\circ$ ), whereas laminates RFL4 and RFL5 present a somewhat lower values ( $\bar{\theta}_I \approx 3.6^\circ$ ). Both RTM laminates as discussed previously were recorded to have the smallest amount of out-of plane fibre misalignment, equal to  $\bar{\theta}_I \approx 3.2^\circ$ . On the other hand, in terms of in-plane waviness, both RTM and RFI panels exhibited only marginal differences. Only RFL1, RFL2 and RFL4 laminates were identified to have somewhat higher values,  $\bar{\phi}_I \approx 2.0^\circ$  which could be due to the possible manufacturing mistakes made in the first panels e.g. preform fabric dislocation during lay-up, due to the initial limited hands-on experience of the author.

| Laminate | $\bar{\theta}_I$ ( $^\circ$ ) | $\theta_{I\max}$ ( $^\circ$ ) | $\bar{\phi}_I$ ( $^\circ$ ) | $\phi_{I\max}$ ( $^\circ$ ) |
|----------|-------------------------------|-------------------------------|-----------------------------|-----------------------------|
| RFL1     | 4.0                           | 7.6                           | 2.0                         | 3.5                         |
| RFL2     | 4.3                           | 7.6                           | 1.7                         | 3.2                         |
| RFL3     | 3.9                           | 7.8                           | 2.1                         | 3.6                         |
| RFL4     | 3.6                           | 7.6                           | 2.0                         | 3.5                         |
| RFL5     | 3.6                           | 7.4                           | 1.7                         | 3.4                         |
| RTM1     | 3.2                           | 7.6                           | 1.9                         | 3.5                         |
| RTM2     | 3.1                           | 7.5                           | 2.0                         | 3.6                         |

Table 3.3: Summary of in- and out-of-plane fibre misalignments per laminate.

# Chapter 4

## Mechanical Testing

### 4.1 Introduction

To numerically model NCF composites with accuracy, a pool of data for various loading conditions had to be generated. It was therefore necessary an array of both standard and non-standard tests to be performed, which included in-plane failure, interlaminar fracture, failure propagation and LVI damage in order to obtain a holistic view of the structural performance of the material system in quasi-static and LVI conditions. The coupon testing began with tests to characterise the basic elastic behaviour of the NCF composites and evaluate the engineering properties that will determine the design allowables. Then, the interlaminar fracture properties were examined through Mode I (DCB) and Mode II (4pt-ENF) tests, while the translaminar fracture properties were assessed with CT test. Due to their special nature, LVI tests are going to be presented in Chapter 9 of the thesis together with the respective FE analyses. Moreover, the stitches of the NCF preform are tested in tension and the respective mechanical properties are obtained. Table 4.1 summarises the tests performed, the test methods, lay-up configuration and the laminates out of which they were cut-off. Fracture surfaces were also inspected in order to identify failure mechanisms and the influence of NCF architecture on ultimate failure. The findings of these analyses will help developing adequate failure criteria which will be incorporated to the developed local models.

| Test                       | Laminate | Test Standard                    | Lay-up           |
|----------------------------|----------|----------------------------------|------------------|
| Polyester stitches-Tension | -        | ASTM D2256 [151]                 | -                |
| Tension                    | RFL2     | ASTM D3039 [136]                 | $[0/90]_{4S}$    |
| In-Plane Shear             | RFL3     | ASTM D3518 [138]                 | $[+45/-45]_{4S}$ |
| Compression                | RFL1     | Haeberle and Matthews [137]      | $[0/90]_{4S}$    |
| Mode I (DCB)               | RFL4     | ASTM D5528 [139]                 | $[90/0]_{7S}$    |
| Mode II (4pt ENF)          | RFL4     | Davidson and Martin [140], [141] | $[90/0]_{7S}$    |
| Compact Tension            | RFL5     | Pinho et al. [73]                | $[90/0]_{14S}$   |

Table 4.1: *Coupon level mechanical testing summary.*

## 4.2 Polyester stitch mechanical characterisation

The mechanical properties of the polyester stitches were obtained by performing tensile tests, according to the ASTM D2256 [151] method after removing them carefully from the preform, in an Instron 5960 test machine supplied with a 300 N load cell. Testing the stitches was considered essential because their tensile properties would be included in the calculation of the mechanical properties of the laminates. The yarns were constrained at a gauge length of 150 mm and tested at displacement rates varying from 100 to 300 mm/min, since the test should not take longer than  $20 \pm 3$  secs from the start [151]. One sample of yarns was tested per displacement rate variant. The principal yarn properties, namely linear density,  $\rho_{linear}$  and areal density,  $\rho_{areal}$ , were provided by the fabric supplier (Cristex Composites Materials Ltd [152], and were equal to  $\rho_{linear} = 8.0$  tex and  $\rho_{areal} = 8$  g/m<sup>2</sup> respectively. Hence, the cross-sectional area of the yarn,  $A_{Pes}$  can be obtained by Eq. 4.1, as well as from OM (Fig. 4.1). In Fig. 4.1 (right) the cross-sectional area of polyester fibres in the yarn are indicated.

$$A_{Pes} = \frac{\rho_{linear}}{\rho_{Pes}} = \frac{8g/1000m}{1.3925g/cm^3} = \frac{8g/10^5cm}{1.3925g/cm^3} = 5.745 \cdot 10^{(-5)}cm^2 = 5.745 \cdot 10^{(-3)}mm^2 \quad (4.1)$$

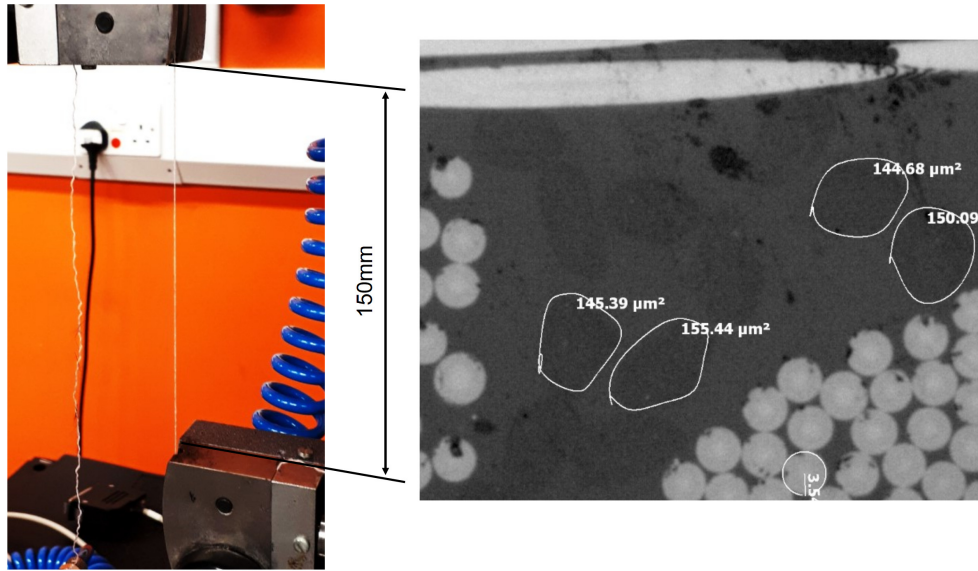
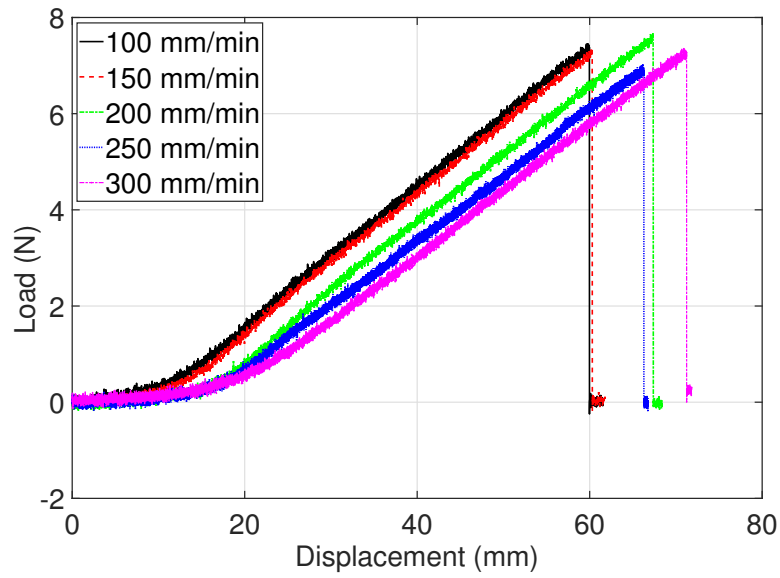


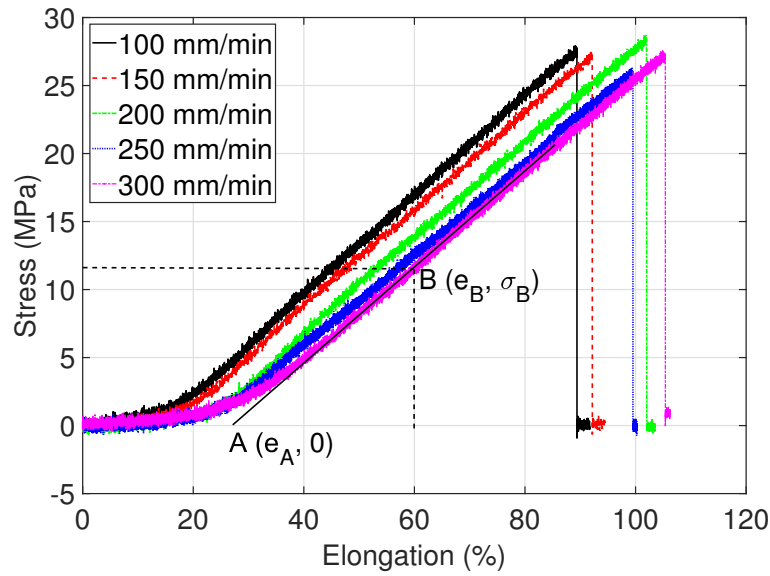
Figure 4.1: Polyester yarn test set-up (left). OM image of polyester stitches, recording the cross-sectional areas of the fibres in the yarn (right).

The load-displacement curve (Fig. 4.2a)) of the polyester yarn reveals rate dependence only within a very small range. Since the cross-sectional area of the yarn is already calculated, the stress-strain response can be produced, out of which the tensile strength,  $X_{T_{Pes}}$  and Young's modulus,  $E_{Pes}$  can be extracted. Usually,  $E_{Pes}$  and  $X_{T_{Pes}}$  are expressed in units of N/tex, however for the present study it is considered more convenient to transform the units to MPa. From the stress-strain plot it was observed that the polyester yarn exhibited a linear response which obeys Hooke's Law. Hence, as suggested by ASTM D2256, the linear region of the curve was extended to the zero-stress axis. This intersection point is the zero-strain point (A) from which strain is measured. The modulus is then determined by dividing the stress at point (B) along the line by the strain at the same point:  $E_{Pes} = (\sigma_B - 0)/(\epsilon_B - \epsilon_B)$  in MPa.

$X_{T_{Pes}}$  is calculated directly from the maximum force divided by linear density (or cross-sectional area) of the yarn, while the Poisson's ratio,  $\nu_{Pes}$  was taken from literature sources [153] equal to 0.25. Table 4.2 summarises the calculated mechanical properties of polyester stitches for the different displacement rates.



(a) Load - displacement curve.



(b) Stress - strain curve.

Figure 4.2: Tensile response of polyester yarn for various displacement rates.

| Yarn    | Rate (mm/min) | $E_{\text{Pes}}$ (MPa) | $X_{\text{TPes}}$ (MPa) |
|---------|---------------|------------------------|-------------------------|
| Y_1     | 100           | 31.5                   | 25.9                    |
| Y_2     | 150           | 32.5                   | 27.4                    |
| Y_3     | 200           | 34.49                  | 28.2                    |
| Y_4     | 250           | 33.71                  | 27.0                    |
| Y_5     | 300           | 33.75                  | 27.0                    |
| Average | -             | $33.2 \pm 1.5\%$       | $27.0 \pm 1.1\%$        |

Table 4.2: Mechanical properties of polyester stitches for different displacement rates.



## 4.3 Tension Testing

### 4.3.1 Specimen Preparation

Tension tests were conducted in accordance with the ASTM D3039 [136] test standards using a  $[0^\circ/90^\circ]_{4S}$  lay-up. The specimens were obtained from RFL\_2 laminate and in order to identify potential damage on the specimens after the waterjet-cut, a second scanning session was performed (Fig. 4.3). Fig. 4.3 presents the tension specimens, which did not reveal any damage after the cutting process.

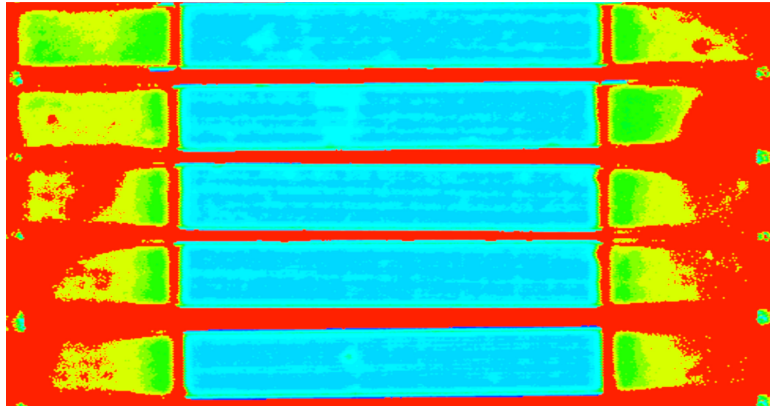


Figure 4.3: *C-Scan image of the waterjet-cut tension specimens from RFL\_2 laminate.*

The dimensions of the tension specimens are presented in Fig. 4.4, while detailed information about the measured dimensions of all five tension specimens can be found in Tab. A.1 of Appendix A.2. Woven glass end tabs have been glued to the specimens using the Araldite 2011 resin epoxy adhesive. In order to achieve a full-field inspection of the damage and fracture phenomena during testing, a GOM Digital Image Correlation (DIC) camera was used in these series of experiments. All tension specimens were speckle patterned on one side in order to create the appropriate surface mapping for the DIC camera during testing. Moisture removal techniques were followed for all specimens according to ASTM D5229 [154].

### 4.3.2 Testing Setup

With the DIC camera the axial and transverse strain and displacement fields were measured and any in- and out-of plane bending effects of the specimens were detected as well. Crack onset

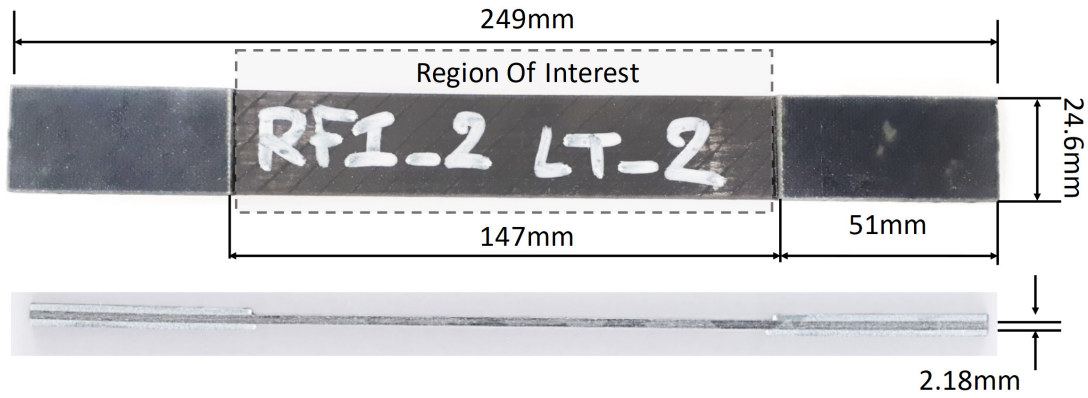


Figure 4.4: *Average dimensions of tension specimens.*

and propagation in the surface plies can also be monitored through DIC and the differences in damage formation between the coupons can be observed. The GOM DIC camera had maximum resolution of 4096 pixels x 3000 pixels, while GOM Correlate software was utilised to postprocess the test results. The Region Of Interest (ROI), as illustrated in Fig. 4.4, was the gauge length of the specimen (146 mm x 25 mm - 2900 pixels x 500 pixels). The pixel resolution was 0.05 mm whereas the facet size (subset size) was set to 19 pixels x 19 pixels and point distance of 16 pixels was used meaning a 50% overlap, as suggested by Aramis for 6 Megapixel CCD cameras. To filter undesired data, a simple mean filter was used in the DIC commercial software Aramis GOM setting value 17. The GOM DIC camera was synchronised with the test machine at 15 Hz via an analogue to digital converter thus the load from the cross-head and the optical displacement curves could be plotted purely from the DIC source. All previously described DIC parameters were verified after performing sample tests and compared against already validated data.

### 4.3.3 Test Results

The tensile tests were conducted in an Instron 5980 test machine of 250 kN capacity equipped with hydraulic wedge grips at a controlled cross-head speed of 1.5 mm/min. As it can be identified from Fig. 4.5 all specimens exhibited a linear response up to ultimate failure. Overall, an acceptable level of consistency ( $CV \leq 5\%$ ) in terms of Young's modulus and tensile strength can be noted, however in the cases that some discrepancy in Young's modulus can be identified between the tested specimens, could be attributed to possible localised in-plane fibre misalign-

ment within the specimen during which an intralaminar stress component is generated in some fibres, resulting in intralaminar (or else intrabundle) failure, leading to a lower ultimate tensile strength of the specimen. The elastic properties (Young's Modulus and Poisson's ratio) were determined in the region of 0.1% to 0.3% axial strain as indicated by ASTM D3039 standards [136]. With the assistance of the DIC camera, the recorded in- and out-of-plane bending effects were significantly less than 10%, which is the premature bending threshold as defined by ASTM D3039 standards. The tensile strength values are calculated as the recorded load at failure, divided by the specimen cross-section area.

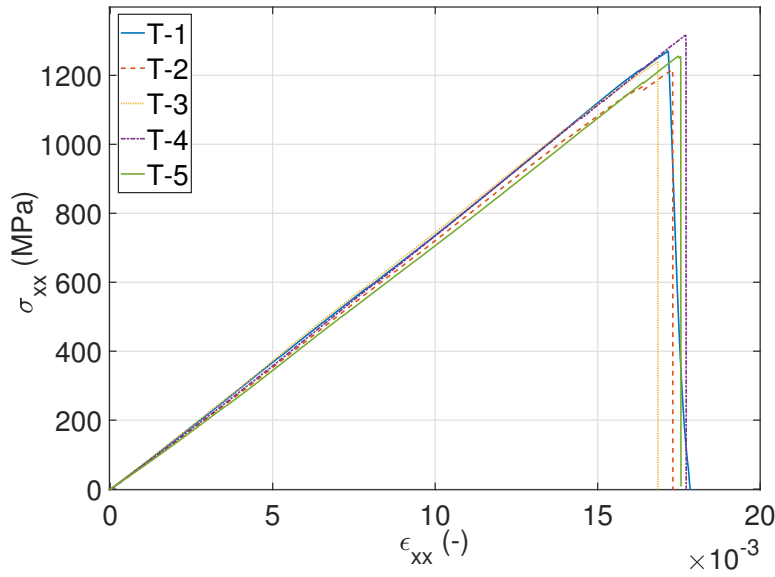


Figure 4.5: *Stress-strain curves of tension specimens.*

Table 4.3 summarises the mechanical properties obtained from the tensile tests, which are correlated with the average measured values of in- and out-of-plane fibre misalignment per specimen. This correlation is indicative to the effect of fibre undulation primarily in longitudinal stiffness and, under conditions, to the tensile strength of the specimen. In particular, the stiffness in the fibre direction under tensile load is found to be essentially influenced by the fibre waviness in both phases. This phenomenon has been widely reported in other studies as well [24, 34, 107, 155]. The irregularity of nominally straight tows introduces flaws that are widely distributed in strength and space, encouraging delocalised failure. Specifically, during tension in the fibre direction, the initially misaligned fibre tows perform a plastic straightening and elongate at approximately constant local axial loads and hence the composite appears

macroscopically to soften for high values of fibre misalignment (e.g.  $> 6^\circ$ ).

| Coupon                  | T_1     | T_2     | T_3     | T_4     | T_5     | Average | CV (%) |
|-------------------------|---------|---------|---------|---------|---------|---------|--------|
| $\theta_I$ ( $^\circ$ ) | 3.4     | 4.4     | 4.0     | 4.5     | 3.8     | 4.0     | 2.7    |
| $\phi_I$ ( $^\circ$ )   | 1.3     | 2.0     | 1.8     | 2.1     | 1.6     | 1.8     | 1.5    |
| $E_{xx}$ (MPa)          | 73437.6 | 70464.0 | 71347.3 | 69550.5 | 72363.5 | 71025.3 | 3.14   |
| $X_T$ (MPa)             | 1316.9  | 1171.3  | 1217.2  | 1184.7  | 1270.2  | 1242.5  | 4.0    |
| $\nu_{xy}$ (-)          | 0.0479  | 0.0515  | 0.0514  | 0.0456  | 0.0446  | 0.0499  | 8.6    |

Table 4.3: *Mechanical properties of tension specimens.*

#### 4.3.4 Fractography

Fig. 4.6 displays the representative failed specimen, where the specimen failed in a brittle manner. Monitoring the data from the DIC in Fig. 4.7, it was observed that, prior to ultimate failure, some localised matrix failure was developed at the top surface of the specimens in the resin rich areas. This is attributed to the stitches which contribute to the failure processes, either acting as initiation sites for damage evolution, as illustrated in Fig. 4.8 (top left) or by debonding from the surrounding matrix and thus promoting matrix failures [156].



Figure 4.6: *Failed tension specimen.*

All tensile specimens exhibited a fibre-dominated pull-out failure (Fig. 4.8 - bottom right), with failure section perpendicular to the loading direction (Fig. 4.8 - top right). This type of failure mechanism is extensively reported in other studies e.g. [39]. When the fibre bundle architecture imparts waviness off-axis local stresses are generated, in particular, the curved layer is not uniaxial and as a consequence is subjected to large normal and tangential tractions at the layer interface. However, the earliest event during tensile loading, well before ultimate failure, has been identified as transverse cracking within the bundles [3,20,34] of the  $90^\circ$  layers; these cracks promote the delamination initiation outside the bundles (Fig. 4.8, top right and bottom left), which ultimately lead to lack of support on the load bearing tows, particularly if they are wavy [157]. The transverse cracks are generally developed at the free edges and spread

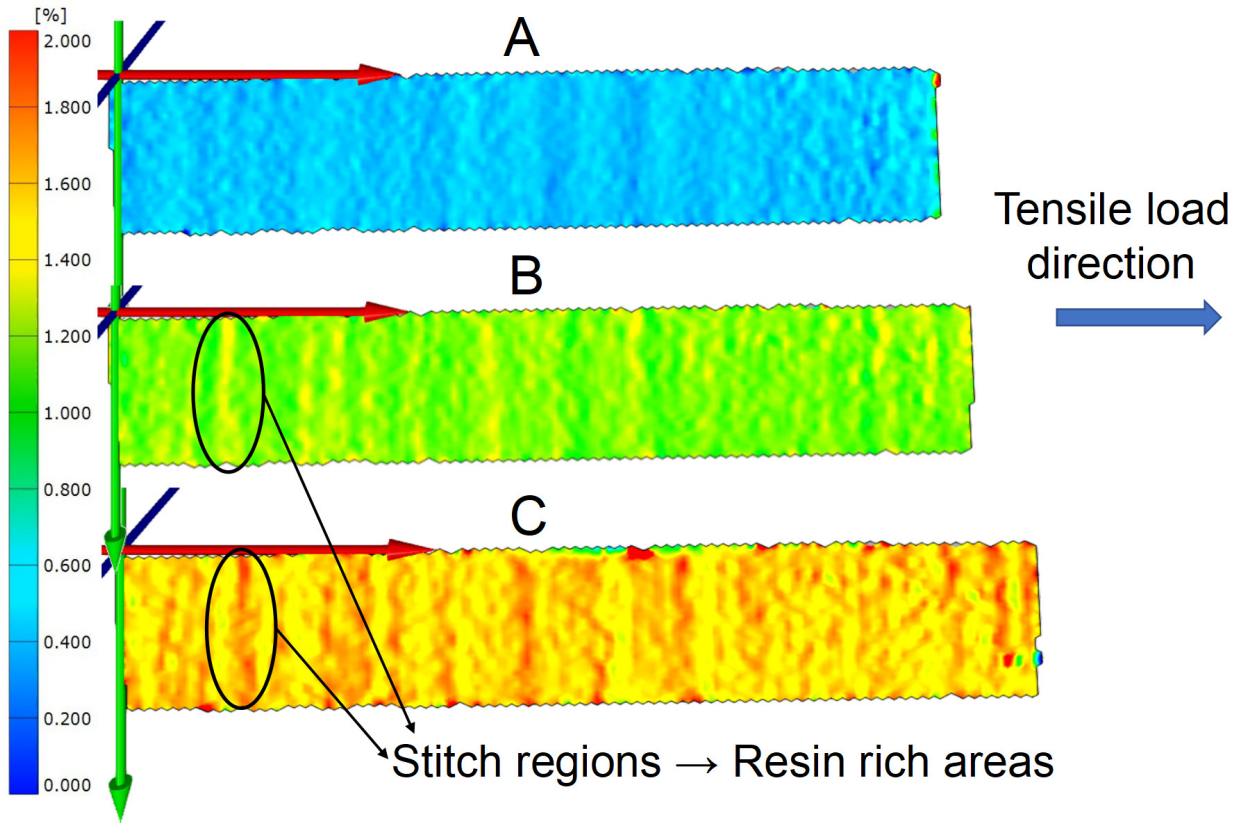


Figure 4.7: View of the tensile specimen as obtained from DIC depicting the matrix at three discrete strain values, A: 0.006, B: 0.014 and C: 0.018.

inwards to the centre of the specimen [50] (indication of white arrows in top right Fig. 4.8).

## 4.4 In-plane Shear Testing

A full in-plane shear characterisation is necessary for the accurate numerical modelling of more complex loading conditions e.g. of which is low velocity impact [158]. The in-plane shear composite characterisation utilised the ASTM D3518 [138] standard, where the specimens were loaded axially at a stroke of 2 mm/min. Similarly to the tensile tests, the in-plane shear tests were carried out in an Instron 5980 test machine of 250 kN capacity with hydraulic wedge grips. Also, a GOM DIC camera was used during this set of tests to measure the shear strain and displacement fields and detect any premature bending effects of the specimens as well.

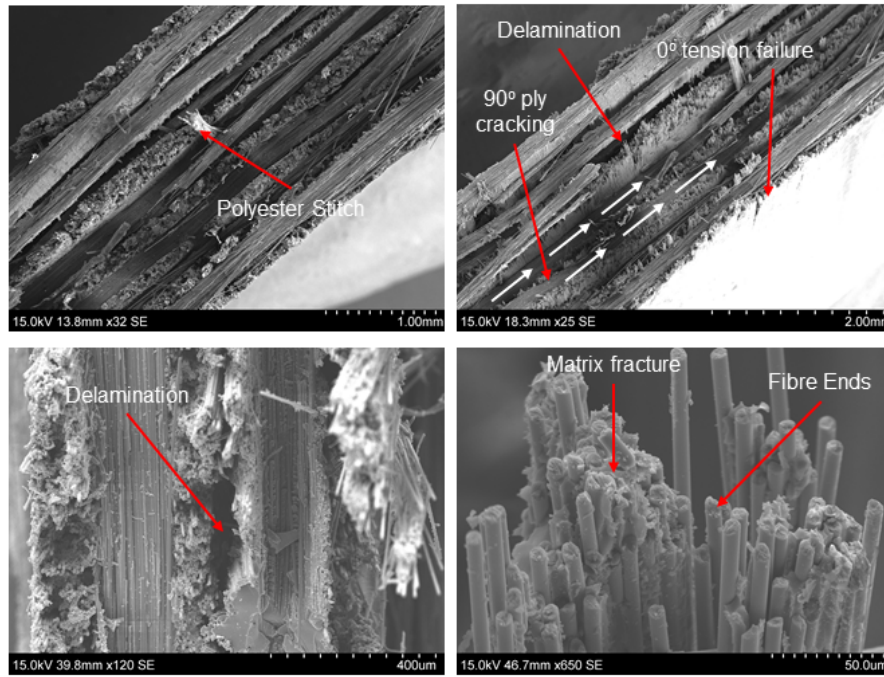


Figure 4.8: SEM illustrations showing: stitch-matrix debonding (top left, magnification:  $\times 32$ ), tension failure (top right, magnification:  $\times 32$ ), delamination (bottom left, magnification:  $\times 120$ ) and matrix fracture and fibre pull-out at tow level (bottom right, magnification:  $\times 650$ ).

#### 4.4.1 Specimen Preparation

The in-plane shear specimens were waterjet cut from laminate RFL3 in a  $[45^\circ/-45^\circ]_{4S}$  lay-up and then C-scanned to detect potential damage caused during the cutting process (Fig. 4.9). The specimen preparation of the in-plane shear specimens was identical to the preparation procedure for the tension specimens. Fig. 4.10, shows the average dimensions of the specimen, whereas in Tab. A.2 of Appendix A.2 the exact dimensions of all specimens are summarised. Lastly, the in-plane shear specimens were speckle patterned on one side in order to create the appropriate surface mapping for the DIC camera during testing.

#### 4.4.2 Testing Setup

The DIC camera was setup identically to the tension case with sole difference the lower recording frequency which was set to 5 Hz due to longer duration of the tests.



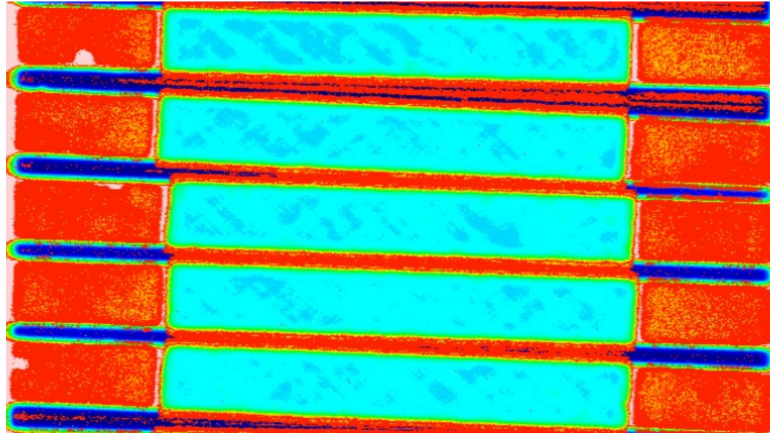


Figure 4.9: C-Scan image of the waterjet-cut in-plane shear specimens from RFI\_3 laminate.

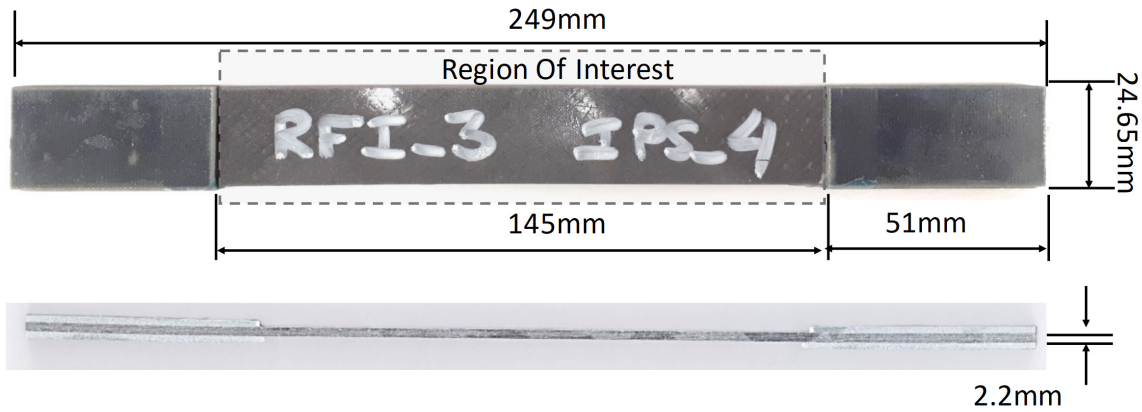


Figure 4.10: Average dimensions of in-plane shear specimens.

#### 4.4.3 Test Results

Five specimens were tested under in-plane shear and their mechanical response is presented in Fig. 4.11 in terms of representative shear stress versus shear strain curves, which are characterised by the high non-linearity beyond the initial linear stage ( $\gamma_{xy} = 0.005$ ). This is attributed to the matrix dominated character of shear failure, where the gradual degradation of matrix during testing is responsible for the non-linear response of the specimens. With the assistance of the DIC camera, the recorded in- and out-of-plane bending effects were significantly less than 10%, which is the premature bending threshold as defined by ASTM D3518 standards.

Table 4.4 summarises the resulting material properties, namely shear modulus ( $G_{xy}$ ) and shear strength ( $S_T$ ). The shear modulus is determined in the region of 0.2% to 0.6% shear strain as indicated by ASTM D3518 standards [138]. The consistency of the test results is proved by the

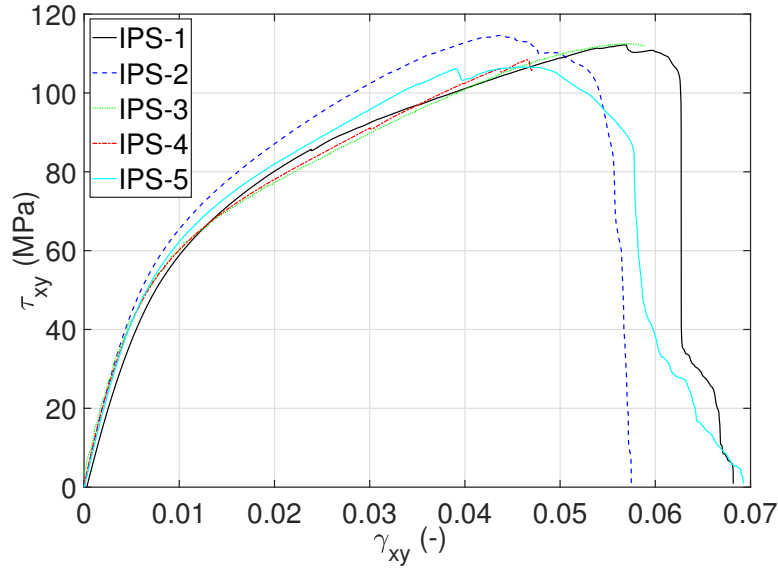


Figure 4.11: *Stress-strain curves of in-plane shear specimens.*

low values of coefficient of variation both for shear modulus and shear strength.

| Coupon | $G_{xy}$<br>(MPa) | $\overline{G_{xy}}$<br>(MPa) | CV<br>(%) | $S_T$<br>(MPa) | $\overline{S_T}$<br>(MPa) | CV<br>(%) |
|--------|-------------------|------------------------------|-----------|----------------|---------------------------|-----------|
| IPS-1  | 8953.83           |                              |           | 109.37         |                           |           |
| IPS-2  | 8591.91           |                              |           | 113.99         |                           |           |
| IPS-3  | 8752.09           | 8829.75                      | 4.12      | 109.80         | 112.27                    | 1.98      |
| IPS-4  | 8446.76           |                              |           | 112.26         |                           |           |
| IPS-5  | 8747.85           |                              |           | 113.63         |                           |           |

Table 4.4: *Mechanical properties of in-plane shear specimens.*

#### 4.4.4 Fractography

The main failure mode identified in the specimens is scissoring, initiated though at different shear strain ranges from specimen to specimen,  $\gamma_{xy} = 0.04 - 0.05$  prior to the ultimate failure. As scissoring is defined the fibre reorientations due to pure shear deformation, where the intern fibre tow angle changes due to matrix driven failure (either as fibre breakage and/or fibre/matrix debonding) in the pure resin pockets while it is assumed that fibre tows do not slip at their pivoting points [159]. This behaviour significantly reduces laminate shear stiffness, prolongs shear strain failure [160], and is illustrated through the DIC observations and measurements in Fig. 4.12. The magnitude  $|\Delta w|/w$  defines the relative change of the width of the specimen because of the scissoring, which is a considerable phenomenon in in-plane shear tests [161].



The high values of the contour represent the regions with high relative displacement which corresponds to localised damage (intralaminar and fibre/matrix debonding) of the  $+45^\circ$  fibres and hence to the areas where scissoring tends to initiate. It was later observed in the post-mortem analysis (with the assistance of stereomicroscopy) that the areas with the excessive relative displacement were coincident to the fibre direction at  $+45^\circ$ . In order though to make scissoring more obvious, the range of the  $|\Delta w|/w$  contour was narrowed so that these areas were emphasised.

Based on the procedure above, the solid line was decided to match the high values of the contour, implying that it matches with the  $+45^\circ$  fibre orientation (prior to scissoring). However, the  $-45^\circ$  layer cannot be monitored directly by the DIC, so for the top view of Fig. 4.12 the dashed line is assumed to represent the fibre orientation of the  $-45^\circ$  based on the evolution of the excessive local strains. From the DIC camera, it was detected that the relative angle between the  $+45^\circ$  fibre tows and the  $-45^\circ$  for the above mentioned shear strains is reduced to  $78^\circ$  from  $90^\circ$  (angle between white dashed and solid lines). This approach implies that no rotation of the  $-45^\circ$  layer occurs, however further investigation is considered necessary.

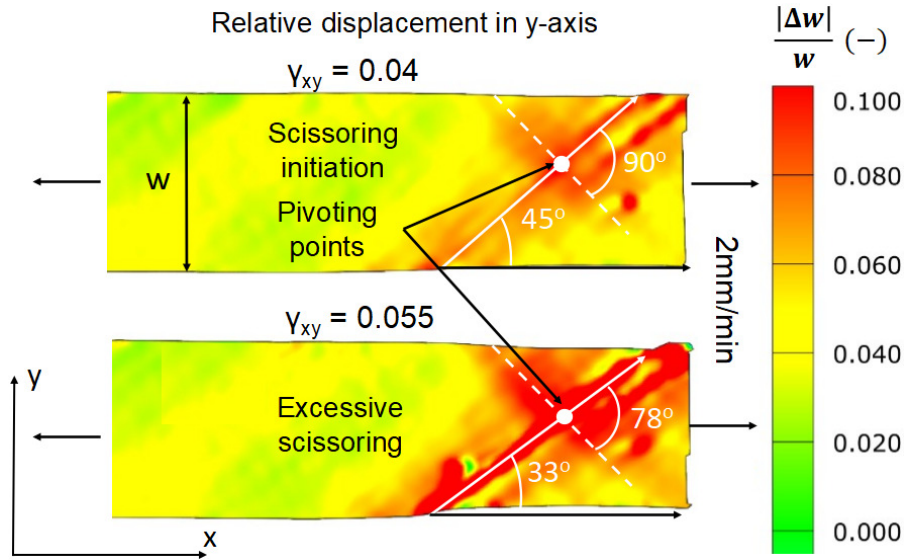


Figure 4.12: *DIC contour of the relative displacement between the  $\pm 45^\circ$  fibre tows in the y-axis (scale: 1:1). Top: Initiation of scissoring at  $\gamma_{xy} = 0.04$ . Bottom: Scissoring evolution at  $\gamma_{xy} = 0.055$ .*

Moreover, shear failure is generated by inducing intralaminar shear within the NCF blankets and delamination between them (Fig. 4.13 - left). In addition to these failure modes, blan-

ket/layer splitting occurs in the form of broom/brush-like failure modes (Fig. 4.13 - left). This is a common shear failure mode in materials with poor fibre/matrix interface strength like NCF composites which then results to stepped fracture of fibres (Fig. 4.13 - right).

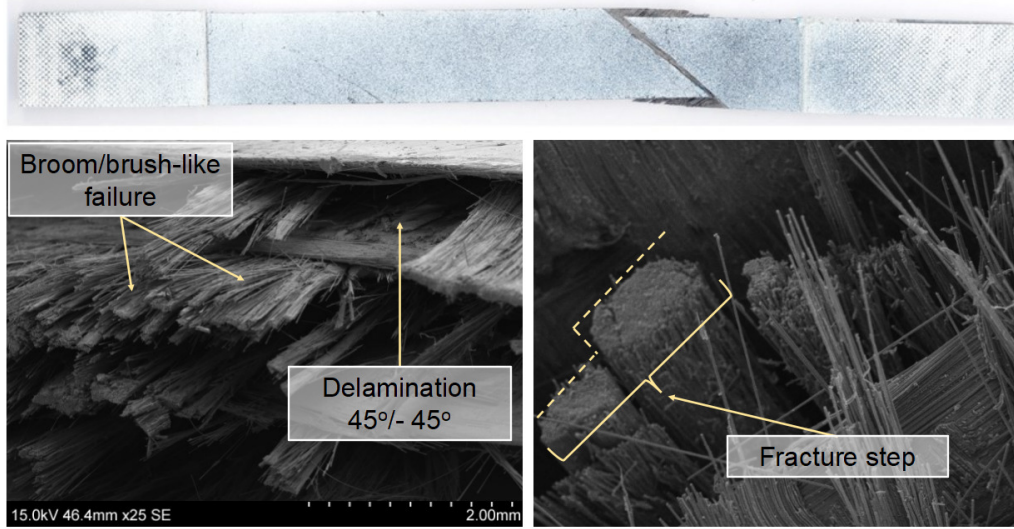


Figure 4.13: Top: Failed specimen after in-plane shear test. Bottom: SEM images of delamination between  $\pm 45^\circ$  blankets and broom-like intralaminar failure (left, magnification:  $\times 95$ ) and stepped fracture of fibres (right, magnification:  $\times 300$ ).

## 4.5 Compression Testing

As described in Chapter 2, the performance of NCF composites under compression is challenging since it is highly susceptible to the fibre undulations that characterise the internal structure of NCF composites. Therefore, in order for their compressive behaviour to be better comprehended, the author decided to test in compression a number of specimens with deliberate in-plane fibre misalignment. In order to alleviate the probability of inconsistency in terms of the volume fractions of the constituents, the compression specimens were intentionally obtained from the same laminate. Specifically, the compression specimens were cut at 6 different in-plane orientations i.e.  $0^\circ$ ,  $2^\circ$ ,  $4^\circ$ ,  $6^\circ$ ,  $8^\circ$  and  $10^\circ$  from laminate RFI\_1 as illustrated in Fig. 4.14. The lay-up configuration of the specimens was formed to a symmetric  $[0^\circ + \phi_I/90^\circ + \phi_I]_{4S}$  lay-up where " $\phi_I$ " denotes the deliberate in-plane fibre misalignment in degrees.

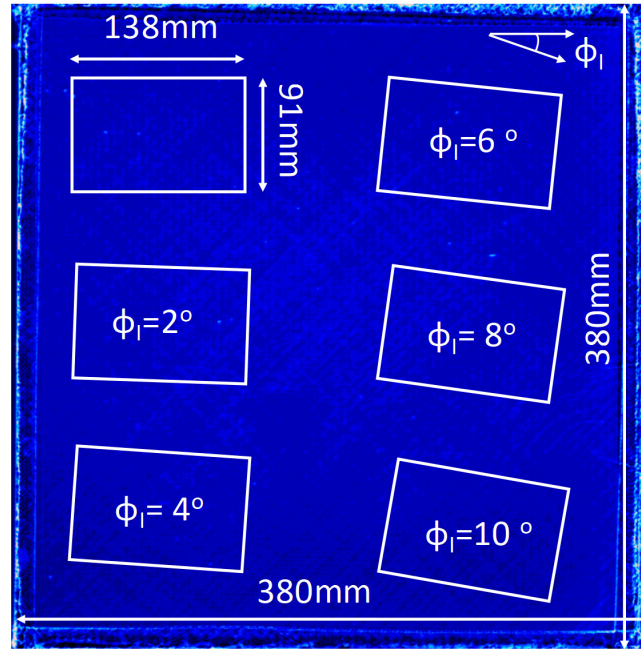


Figure 4.14: Blocks of compression specimens deliberately misaligned on laminate RFI\_1.

#### 4.5.1 Specimen Preparation

The specimens were waterjet cut from RFI\_1 but due to their small size, they were excluded from the C-scan procedure. Their nominal dimensions are 90 mm x 10 mm x 2 mm (Fig. 4.15) and the exact dimensions are presented in Tabs. A.3, A.4, A.5, A.6, A.7 and A.8 of Appendix A.2. The end-tabs of the specimens are from woven glass fibre fabric and epoxy resin. The surfaces of the specimens, where the end tabs were bonded, were cleaned using air pressured sand and then the end tabs were glued to the specimens by Araldite 2011 [162], a two compound epoxy adhesive. Also, linear strain gauges (FLAB-2-11-1LJC-F) were used at each face of the specimen to monitor possible bending effects during compression tests.

In Fig. 4.16 magnified images of the gauge length of the specimens for different angles of fibre misalignment are illustrated.

#### 4.5.2 Testing Setup

The compression tests were performed in an Instron 5960 test machine of 50 kN capacity with mechanical wedge grips at a controlled cross-head speed of 1 mm/min, in accordance with the

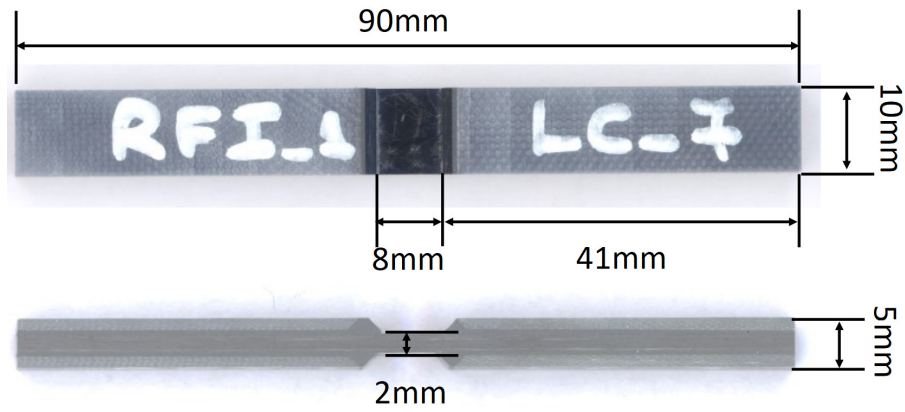


Figure 4.15: *Nominal dimensions of the compression specimens.*

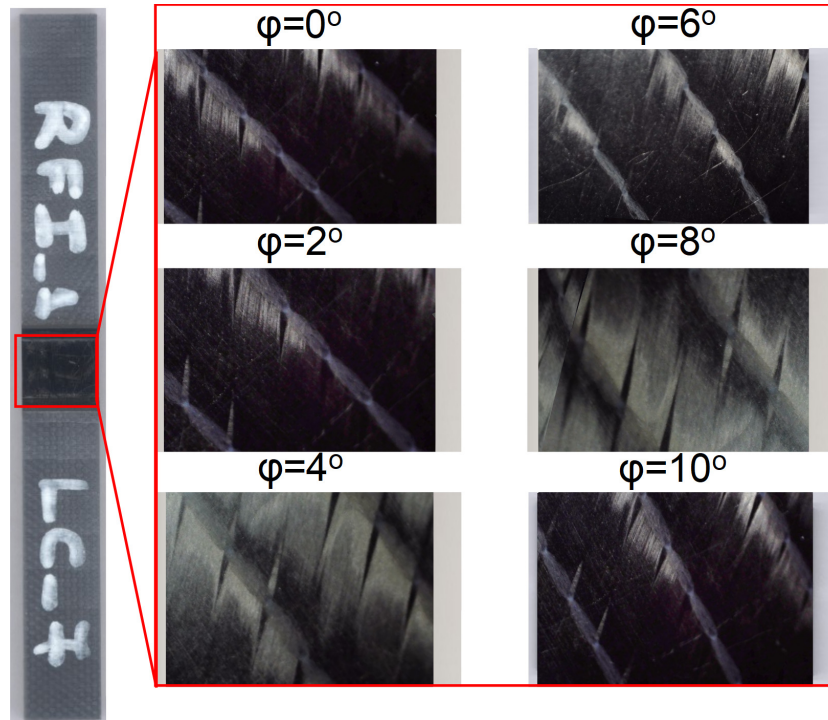


Figure 4.16: *Image of the gauge length area on the compression specimens with deliberately misaligned NCF blankets.*

IC compression method [137]. A Phantom Miro M310-6188 high speed camera of 1152 pixels x 512 pixels resolution was used to record all tests and assist in the identification of the failure mechanisms' sequence during post processing. The high speed camera was set at a sample rate of 10000 fps, (i.e. with interval time of 100  $\mu$ s) and exposure time 4.98  $\mu$ s, together with two Dedocool DC anti-flicker lights targeted at the side of the gauge length of the specimens.

### 4.5.3 Test Results

In total 30 specimens were tested, five per misalignment configuration. Figs. 4.17, 4.18, 4.19, 4.20, 4.21 and 4.22 demonstrate the stress - strain response of the specimens for the different values of in-plane fibre misalignment. For all configurations, a non-linear behaviour is observed which suggests the development of permanent damage or plastic deformation before the ultimate failure [38]. As expected, the comparison between the configurations from graphs highlights that damage onset is later in the specimens with the smaller amount of misalignment, but all cases are characterised by fast unstable damage propagation. The compressive elastic modulus is extracted from the stress strain curves and evaluated from the mean strain of the two linear strain gauges between 0.1 and 0.3% according to the standards [137]. The compressive strength values are calculated as the recorded load at failure, divided by specimen's cross-sectional area. The bending effects for all the specimens which are presented in the following results were calculated as the difference between the strain values along the gauge length as obtained from the strain gauge on the top surface of the specimen and the respective values from the strain gauge on the bottom surface of the specimen. The bending effects were recorded to be within the acceptable limits ( $<10\%$ ).

Small discrepancy in terms of compressive strength is noted for the specimens without deliberate misalignment (Fig. 4.17), whereas for the specimens of the configuration with  $2^\circ$  fibre misalignment (Fig. 4.18), narrow scatter is also observed for the longitudinal stiffness, namely the slope of the stress-strain curves.

However, it appears that as long as the in-plane fibre misalignment increases (Figs. 4.18, 4.19, 4.20, 4.21 and 4.22), the stress-strain response seems to deviate even for specimens of the same misalignment configuration. This behaviour implies that probably out-of-plane fibre undulation exists at different scales for each specimen, which can not be easily detected, and thus result in different mechanical behaviour. Through this variation of the mechanical response for specimens with "nominally" the same characteristics, the uncertainty that describes the internal structure of NCF composites is revealed.

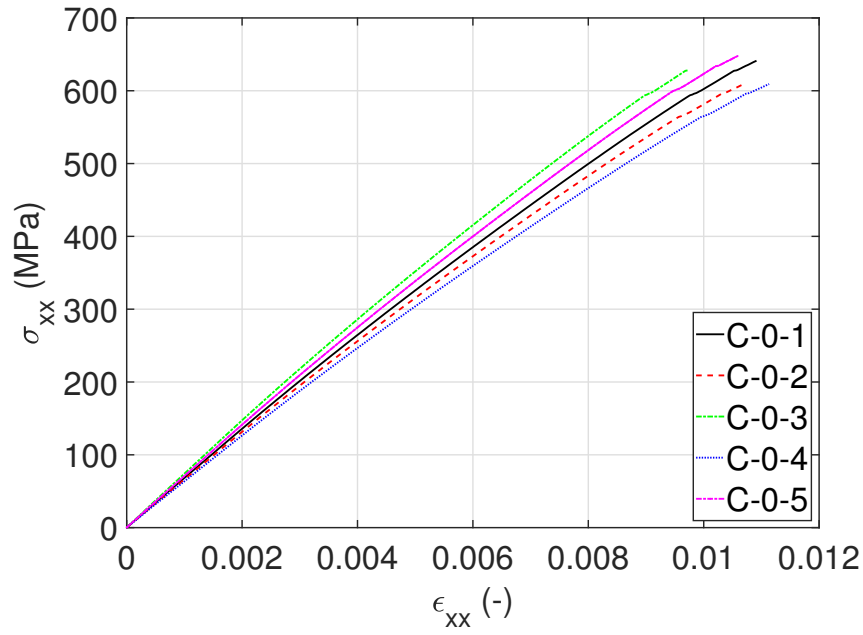


Figure 4.17: *Stress - Strain curves of compression specimens without deliberate in-plane fibre misalignment.*

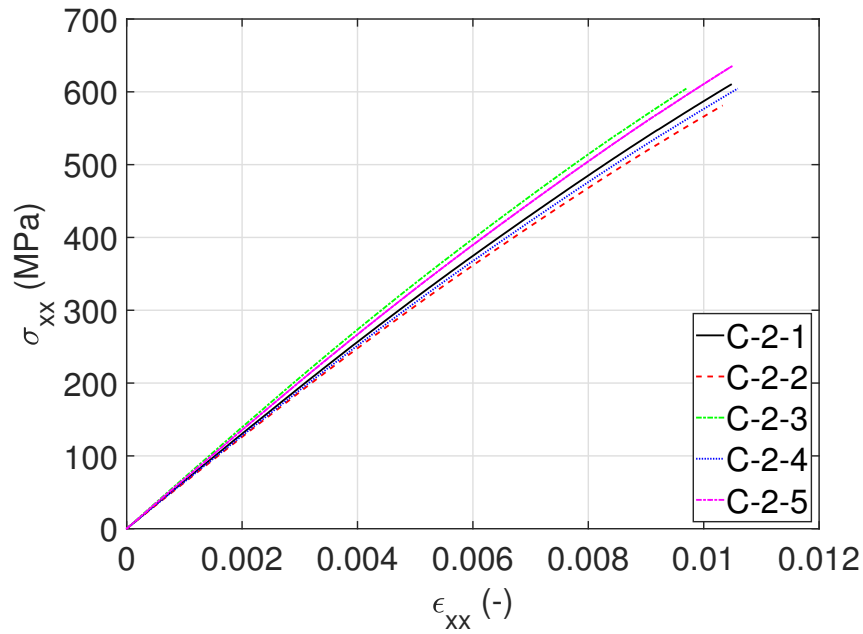


Figure 4.18: *Stress - Strain curves of compression specimens with 2° in-plane fibre misalignment.*

Specifically, the modulus for non-misaligned specimens is ranging from 63 to 73.7 GPa corresponding to  $CV = 4.0\%$ , whereas the compressive strength varies from 609 to 653 MPa. Respectively, for the  $2^\circ$  configuration, the range of longitudinal stiffness varies between 63.7 to 69.6 GPa and the compressive strength from 581 to 634 MPa. The scatter remains noticeable



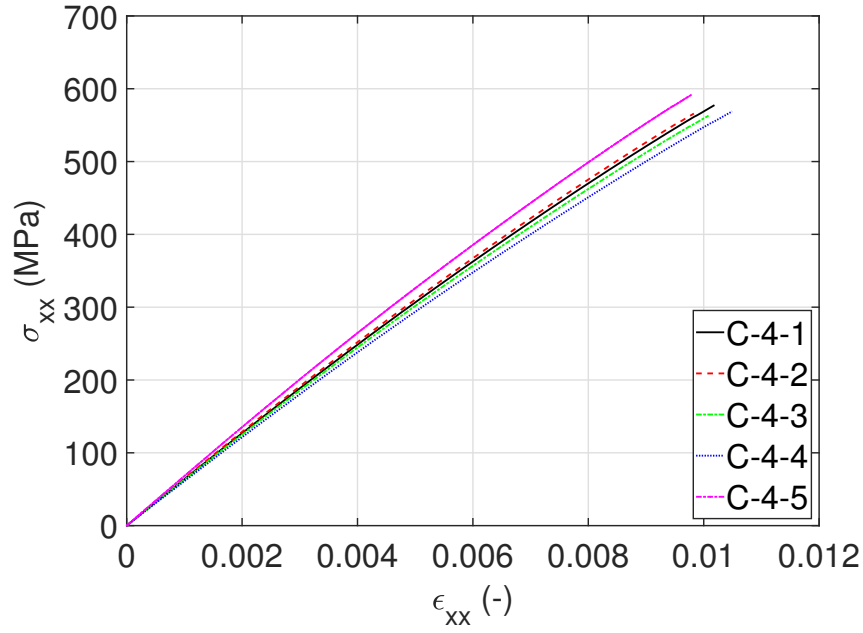


Figure 4.19: *Stress - Strain curves of compression specimens with 4° in-plane fibre misalignment.*

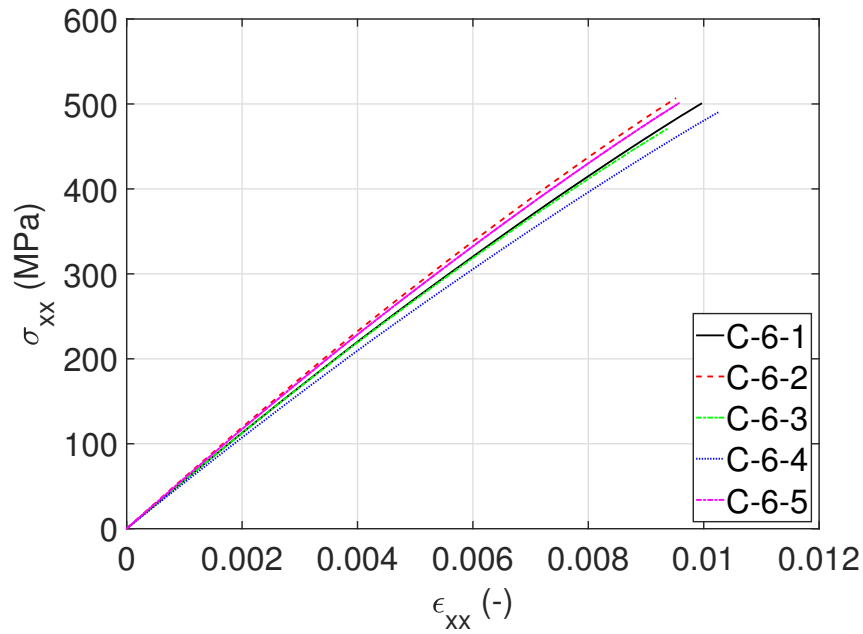


Figure 4.20: *Stress - Strain curves of compression specimens with 6° in-plane fibre misalignment.*

for the 4° configuration (CV = 3.5%), in which both the longitudinal stiffness and compressive strength exhibited the following values, 60.5 to 67.5 GPa and 563 to 591 MPa respectively.

With regards to other configurations, for 6° and 8° in-plane misalignment the compressive

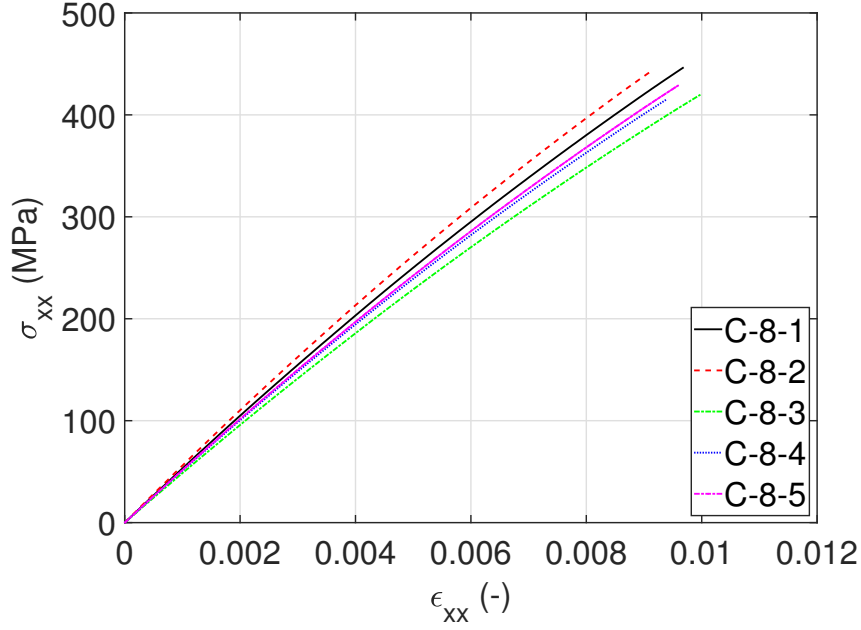


Figure 4.21: *Stress - Strain curves of compression specimens with 8° in-plane fibre misalignment.*

stiffness lies from 53.5 to 59.7 GPa and 47.4 to 54.7 GPa respectively, while the compressive strengths ranging from 470 to 507 MPa and 415 to 446 MPa. A wider deviation of the compressive response among the specimens of 10° misalignment is noted, namely the stiffness ranges from 40.8 to 50.3 GPa with corresponding strength from 346.3 to 396 MPa. It is apparent from the previous results that, in terms of strength, a clear correlation is noted with stiffness, mainly because of the differences in fibre waviness between the specimens. Such observations have been reported by other studies as well [163], concluding that for large variations in fibre waviness larger percentage difference is noted between the minimum and maximum values of strength for specimens of the same configuration. Moreover, it is worth mentioning that the compressive moduli are seen to vary to the tensile moduli. This reduction in compressive moduli can be attributed to the crimp of the fibres which promoted micro-buckling. The average values of the mechanical properties for each tested configuration are summarised in Tab. 4.5.

In Fig. 4.23 a comparison is made between the stress-strain curves closer to the average response for each of the different in-plane fibre misalignment configurations. Comparing the specimens with the increased misaligned lay-up, it is noticed that the longitudinal stiffness is reduced by 3.3 % and 6.3 % for  $\phi_I = 2^\circ$  and  $4^\circ$  respectively whilst for  $6^\circ$ ,  $8^\circ$  and  $10^\circ$  a steeper



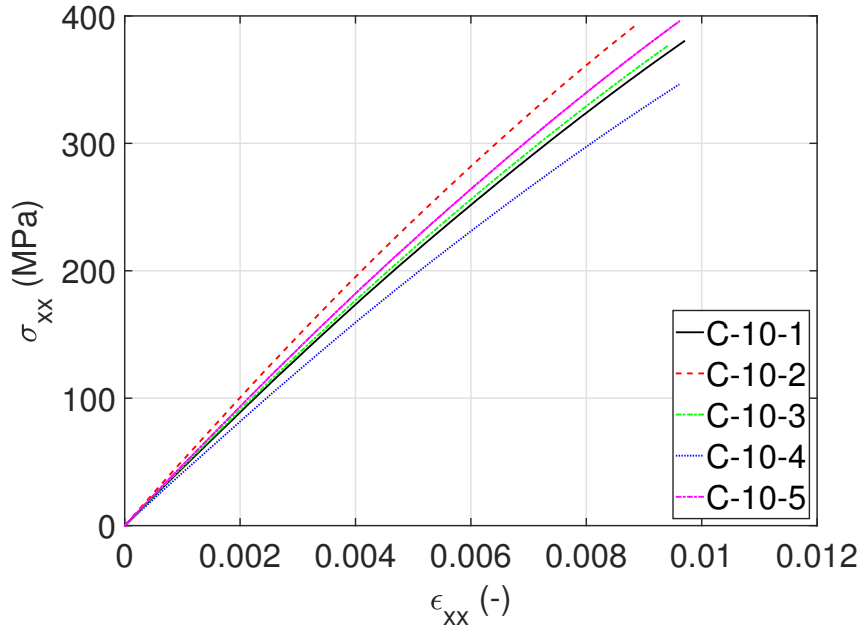


Figure 4.22: *Stress - Strain curves of compression specimens with 10° in-plane fibre misalignment.*

| $\phi_I$ (°) | $\theta_I$ (°) | $E_{xxC}$ (MPa) | CV (%) | $X_{CL}$ (MPa) | CV (%) |
|--------------|----------------|-----------------|--------|----------------|--------|
| 0            | 2.3            | 66573           | 4.0    | 641.2          | 3.7    |
| 2            | 2.8            | 64340           | 4.2    | 608.1          | 3.8    |
| 4            | 2.5            | 62346           | 3.5    | 576.3          | 3.2    |
| 6            | 3.1            | 55012           | 3.8    | 500.9          | 3.4    |
| 8            | 3.4            | 50829           | 3.4    | 446.6          | 3.0    |
| 10           | 4.0            | 43403           | 3.2    | 379.6          | 2.8    |

Table 4.5: *Comparison of the mechanical properties between the specimens of different misalignment configurations.*

reduction up to 34.8 % is exhibited, as summarised in Tab. 4.6. This behaviour is not only attributed to the increased in-plane misalignment but also to the recorded higher values of  $\theta_I$  for those specimens. As expected, the ultimate compressive failure stresses showed a similar trend, however it appears to be more susceptible to the fibre misalignment in both planes, since the reduction of strength reached higher values in terms of percentage, namely from 6% for  $\phi_I = 2^\circ$  to as low as 40% for  $\phi_I = 10^\circ$ .

A simple comparison of the mechanical test results is performed against the predictions of the Classical Laminate Theory (CLT) for the same lay-up and adjusting the orientation of plies

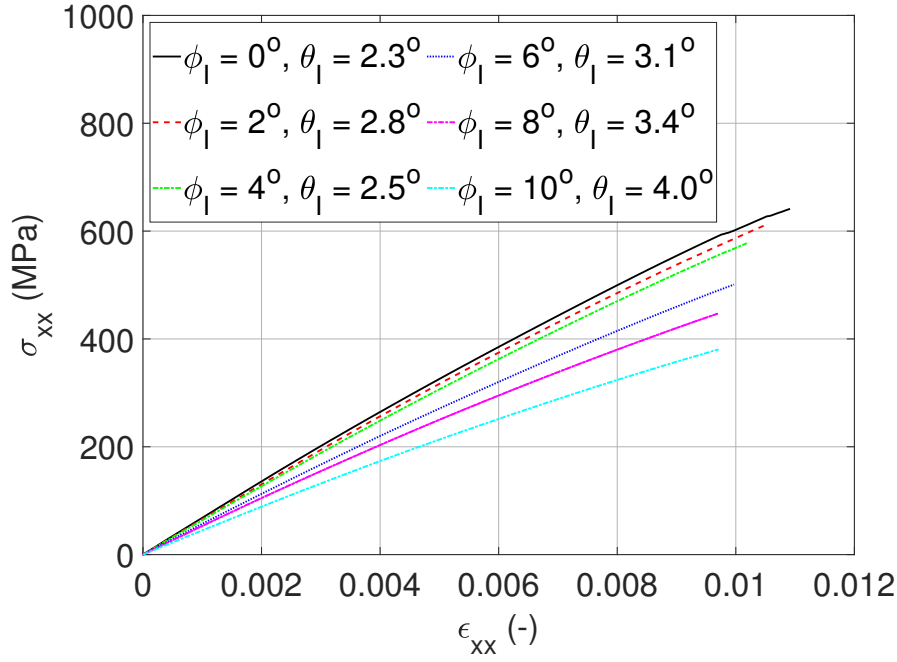


Figure 4.23: Comparative representation of the average stress-strain curve for the specimens with different fibre misalignment.

according to  $\phi_I$ . The necessary mechanical properties at ply level which were introduced to the CLT calculations, namely  $E_{11}$ ,  $E_{22}$ ,  $G_{12}$  and  $\nu_{12}$  were calculated from the methodology later presented in Section 5.2, while the ply thickness was taken equal to 0.137 mm. The material properties are summarised in Table B.6 of Appendix B. The difference in percentage between the values of  $E_{xxC}$  from the compression test and the CLT predictions are shown in the rightmost column of Tab. 4.6, where an initial discrepancy of approximately 10 % is noted which reaches the value 18 % for in-plane fibre misalignment equal to 10°. This difference can be attributed to the fact that the CLT does not account the out-of-plane fibre misalignment,  $\theta_I$  and assumes perfectly bonded plies, hence stiffness reduction due to potential interlaminar damage cannot be predicted. Moreover, although in-plane fibre misalignment,  $\phi_I$ , is introduced to the CLT as ply orientation, it is assumed to be the same in every instance of the specimen whereas in the actual specimen, higher values of in-plane misalignment may exist in the inner blankets of the lay-up. Additional matrix dominated failure mechanisms (e.g., initiation of fibre splitting) which could be developed due to fibre misalignment, cause reduced longitudinal stiffness but they are not accounted by the CLT.

For the above reasons, the reduction of the longitudinal stiffness as predicted by the CLT due

| CLT          |                 |         | Test            |         | Difference              |
|--------------|-----------------|---------|-----------------|---------|-------------------------|
| $\phi_I$ (°) | $E_{xxc}$ (MPa) | Diff.   | $E_{xxc}$ (MPa) | Diff.   | $\frac{CLT-Test}{Test}$ |
| 0            | 73645           | -       | 66573           | -       | 9.60 %                  |
| 2            | 72459           | 1.61 %  | 64340           | 3.35 %  | 11.20 %                 |
| 4            | 69140           | 6.12 %  | 62346           | 6.35 %  | 9.83 %                  |
| 6            | 64295           | 12.70 % | 55012           | 17.37 % | 14.44 %                 |
| 8            | 58654           | 20.36 % | 50829           | 23.65 % | 13.34 %                 |
| 10           | 52850           | 28.24 % | 43403           | 34.80 % | 17.88 %                 |

Table 4.6: Comparison of the longitudinal compressive stiffness, ( $E_{xxc}$ ) between the test results and CLT predictions for the different values of in-plane fibre misalignment ( $\phi_I$ ).

to  $\phi_I$  - ranging from 1.6 - 28.25 % - is smaller compared to the respective reduction of the actual compression tests (3.5 - 34.8 %). In Fig. 4.24, the evolution of the longitudinal stiffness both for CLT and the mechanical tests is presented.

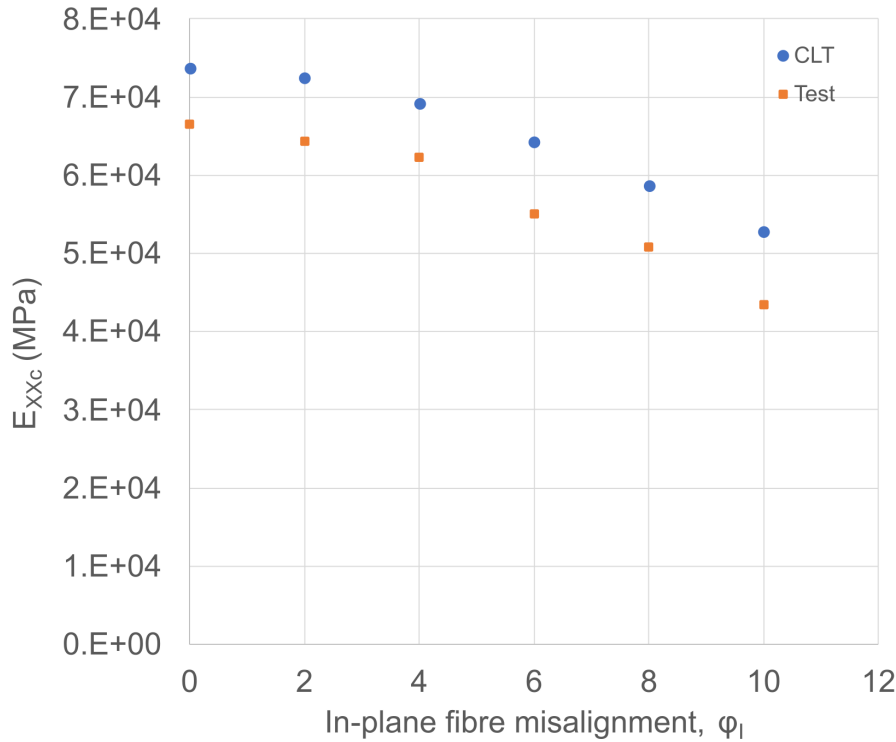


Figure 4.24: Development of the longitudinal stiffness of the laminate ( $E_{xxc}$ ) in relation to in-plane fibre misalignment ( $\phi_I$ ), as obtained from the compression tests (Test) and the CLT.

#### 4.5.4 Fractography

Representative macroscopic failures of the specimens are illustrated in Fig. 4.25a) and b), where Fig. 4.25a) presents a failed specimen of 0° misalignment configuration while Fig. 4.25b) a failed specimen of 10° misalignment. In both pictures the failure can clearly be noticed as an

angled crack through-the-thickness of the specimens accompanied by severe delaminations as a result of the shear driven brittle compressive failure of the  $0^\circ$  and  $90^\circ$  layers. As reported also in other studies [164], the inclined crack surface is a typical behaviour of a shear driven brittle compressive failure.

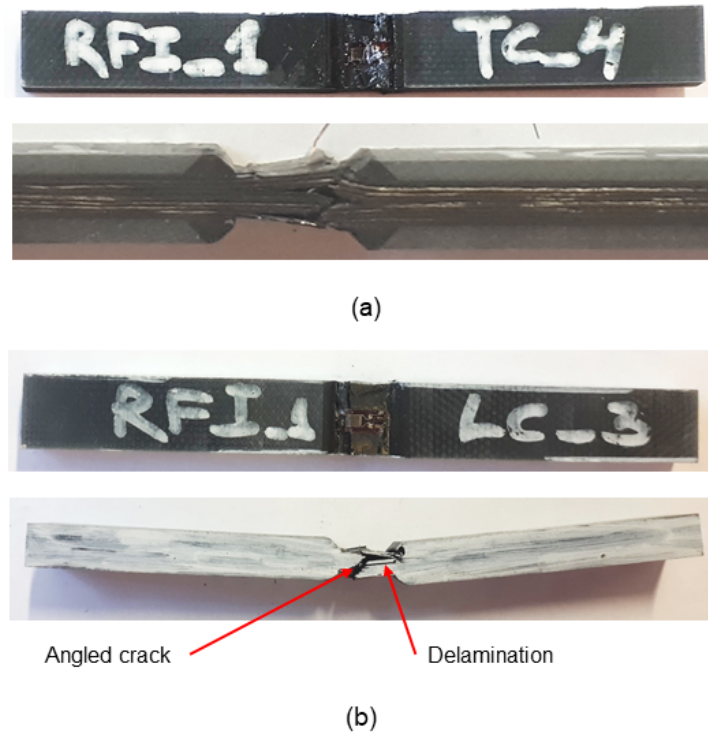


Figure 4.25: Failed specimens in compression of a)  $0^\circ$  and b)  $10^\circ$  in-plane fibre misalignment.

Post-mortem analysis with the SEM was performed, but it can only reveal the final failure mode and not the failure initiation, however as it can be observed, the failure mode can be described as a complex combination of: cracks within fibre bundles, matrix cracking, cracks on the bundle-matrix interface, fibre kinking and fibre splitting [56]. However, it was clearly identified that the dominant failure mechanism for all specimens, regardless the in-plane misalignment, was fibre kinking, generated by localised fibre microbuckling events such as the failure shown in Figs. 4.26 (top right and bottom) and 4.27 (right) [5].

Nonetheless, in configurations with higher crimp, intralaminar and interlaminar delamination can be developed and lead to failures such as those in Figs. 4.26 (top left) and 4.27 (right). The delaminated areas were identified to be in the regions where the out-of-plane fibre undulations were higher. As stated previously, the NCF composites are recognised to be prone to shear

failure when they are subjected to compressive loads. That is due to the limited constraint between the bundles allowing to slide over each other [4]. As the shear component increases, failure starts to be influenced by intralaminar split of the  $90^\circ$  layers. In this failure mode, the in-plane fibre misalignment significantly contributes to the enhancement of the shear component leading to earlier shear failure, therefore the specimens particularly with  $6^\circ$ ,  $8^\circ$  and  $10^\circ$  in-plane fibre misalignment exhibited a significant amount of compressive strength reduction. The  $90^\circ$  layer splitting is sensitive to the out-of-plane fibre misalignment which leads to subsequent delamination between the  $0^\circ/90^\circ$  layers and can be seen in Figs. 4.26 (right) and 4.27 (bottom). The fracture angle at failure was measured to be at approximately  $40^\circ$  for each specimen with respect to the longitudinal direction, as shown in Fig. 4.27 (right).

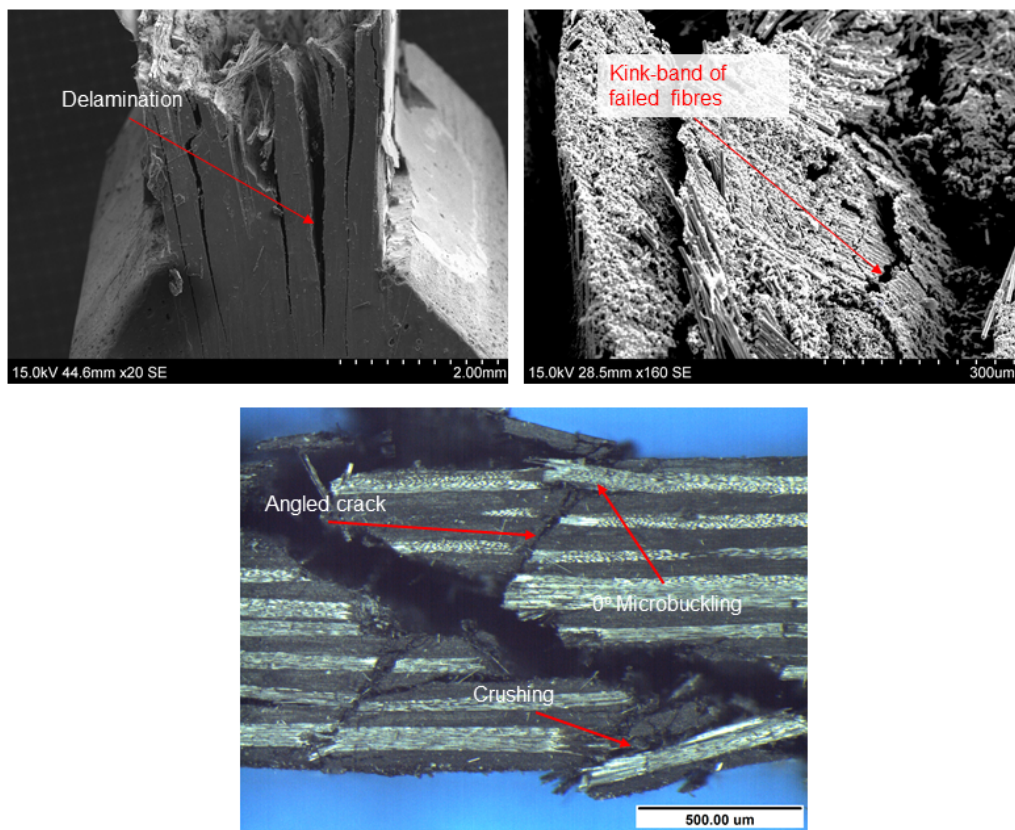


Figure 4.26: *Fractographic analysis of  $8^\circ$  misalignment configuration: Scanning Electron Microscope evidence of delamination (top left) and fibre kinking (top right). Macro level compressive failures obtained from stereo microscope (bottom).*

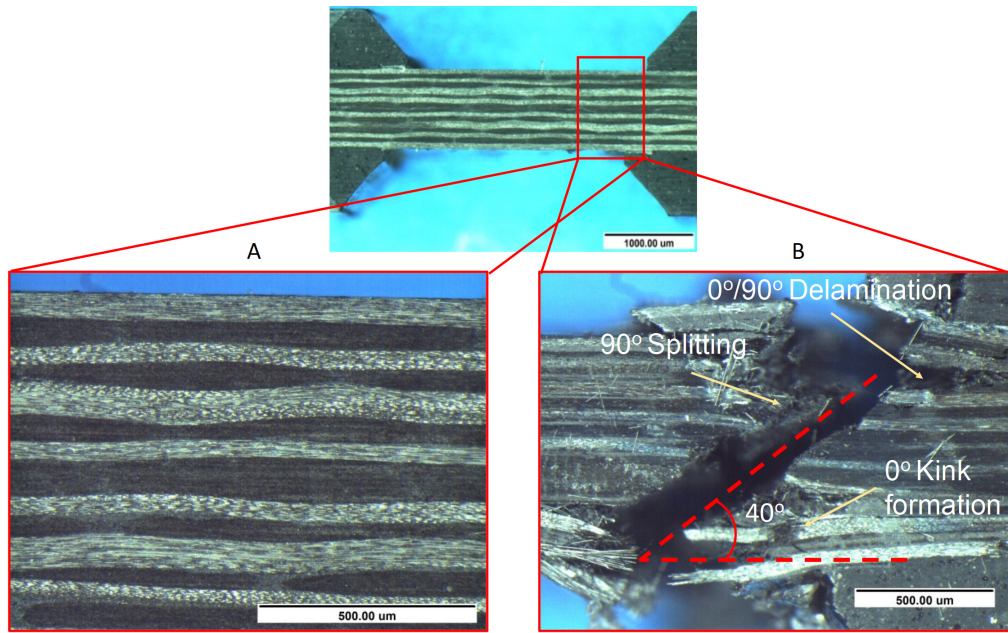


Figure 4.27: Stereo microscope images of a non-misaligned specimen: (Top) Side view of the specimen (scale, 4:1), A: Magnified view before testing (scale: 64:1) and B: Post-mortem image of macro level failures and measurement of fracture angle (right, scale: 45:1).

## 4.6 Mode I - Double Cantilever Beam Testing

ASTM D5528 [139] standard was used for Mode I DCB fracture toughness test through which the interlaminar fracture due to peel is examined. Six specimens were tested in total for one of these a loading-unloading sequence was followed.

### 4.6.1 Specimen Preparation

The DCB specimens were formed by a  $[90^\circ/0^\circ]_{7S}$  lay-up, mirrored around the PTFE insert with the  $0^\circ$  layers aligned to the long dimension of the specimen and situated at the midplane to ensure crack growth along the midsection of the specimen. The average dimensions of the specimens are presented in Fig. 4.28. Two aluminium end blocks with centre holes of 7.50 mm diameter were glued to the specimens with Araldite 2011. The exact dimensions of all DCB specimens are presented in Tab. A.9 (Appendix A.2). Also, the specimens were painted in white, through-the-thickness at one side and a scale with 1 to 5 mm incrementation was marked to facilitate the view of the crack growth using the Immetrum optical strain camera. The test was set-up as shown in Fig. 4.30.



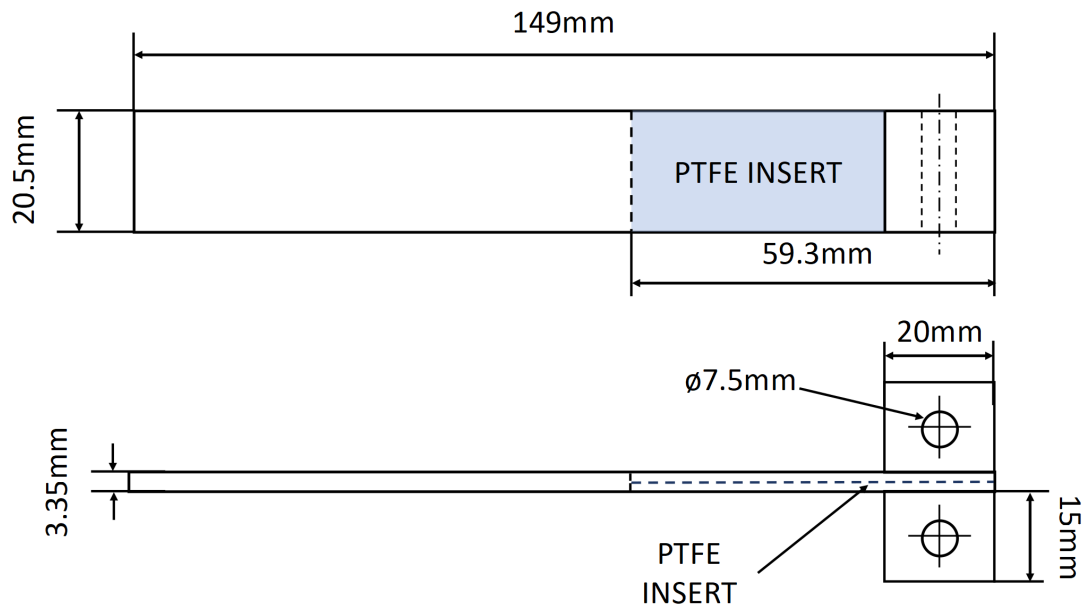


Figure 4.28: *Average dimensions of the Mode I (DCB) specimens.*

A PTFE film insert of 20 microns was placed at the midsection of the laminate to initiate an artificial delamination. The PTFE film was placed approximately 60 mm length inside the laminate and the width of the insert was the same as that of the laminate (Fig. 4.29). The exact position of the PTFE film insert was recorded with the assistance of a stereoscopic microscope.

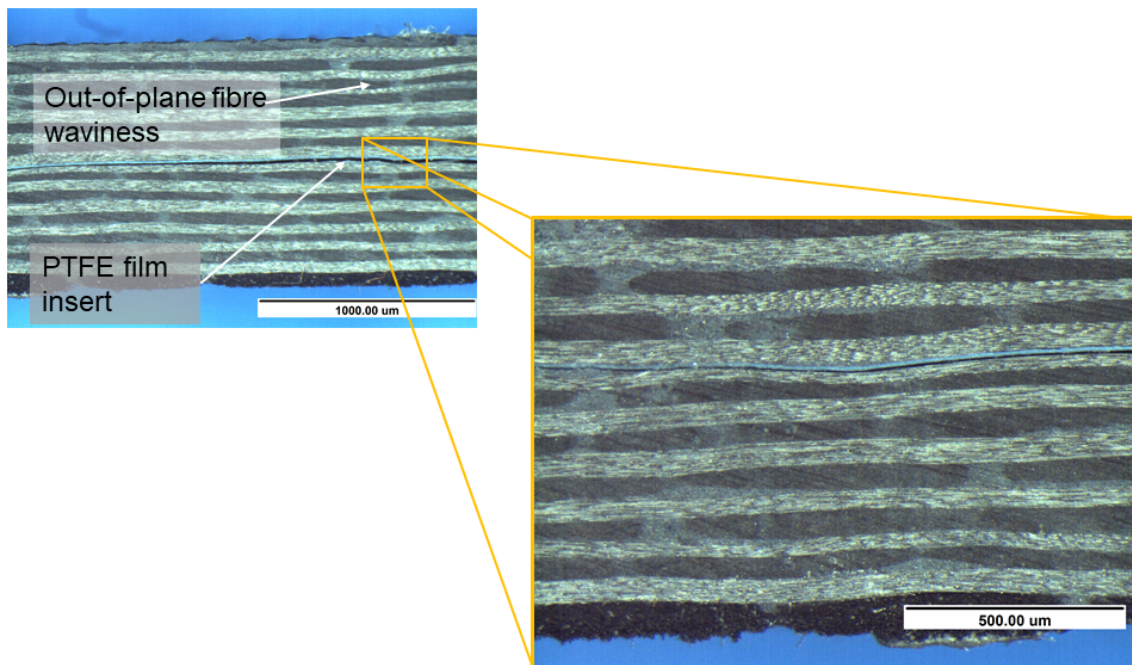


Figure 4.29: *PTFE film insert in Mode I DCB specimen, image obtained from stereoscopic microscope (x32).*

### 4.6.2 Test Setup

The tests were performed in an Instron 5960 test machine of 50 kN capacity at a tensile cross-head speed of 1 mm/min. The Immetrum optical strain camera with pixel resolution of 0.05 mm was set at a frequency as low as 3 Hz and together with the Instron test machine were correlated automatically via an analogue to digital converter thus the load from the cross-head and the optical displacement curves could be plotted purely from the pin locations, minimising the test machine and instrumentation compliance errors. The loading and constrain pins were free to rotate about their axis of symmetry.

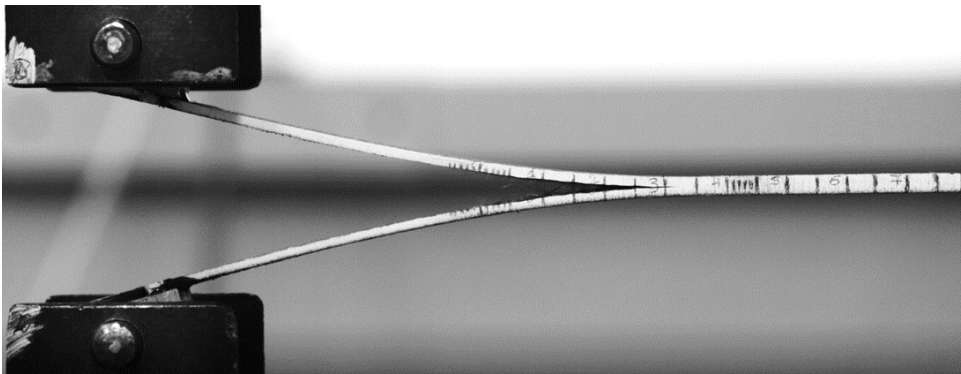


Figure 4.30: *Test set-up for the Double Cantilever Beam.*

### 4.6.3 Test Results

Before actual testing, the specimens were pre-cracked at a stroke of 1 mm/min to a known crack length and then returned to the origin. The specimens were then loaded in tension with the same rate to allow the stick-slip behaviour of the crack propagation to be monitored successfully by the Immetrum optical strain camera and then correlated to the force versus displacement data captured from the test machine. The load versus displacement curves are shown in Fig. 4.31, and reveal that initially there is an increase in the crack propagation load as crack tends to initiate. This is caused by the development of stitch bridging zone since the stitches are nested into the resin of the interface between the arms of the specimen as will be explained later in the fractography sub-section. Once the bridging zone is fully developed, the stitches start breaking and the crack propagation load decreases with increasing crack length. It can be observed, that the crack propagation part of the curves exhibit in general an unstable



behaviour expressed by successive load drops followed by crack arrests [31].

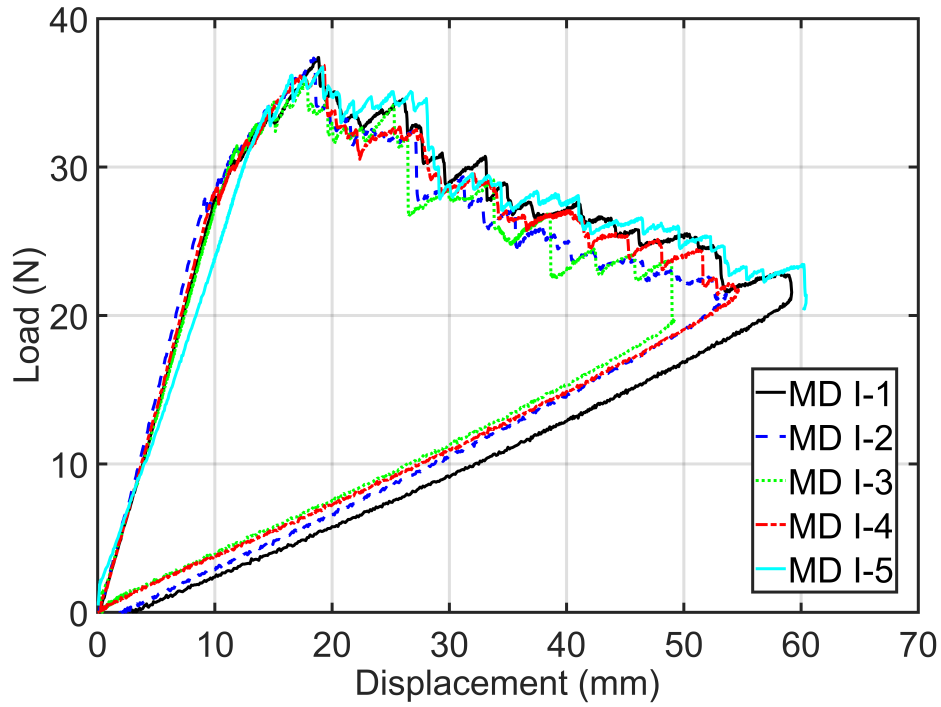


Figure 4.31: *Load - Displacement curves for the Mode I specimens in DCB tests.*

Additionally, a load - unload test was conducted to identify potential permanent displacement of the arms of the specimen and possible hysteresis in the load - displacement response of the specimen. The loading phase of the test was also performed at a tensile displacement rate of 1 mm/min, whereas for the unloading phase a 3 mm/min compressive displacement rate was applied. The load to unload transition was selected to occur at four discrete displacement values, i.e. 20, 30, 40 and 50 mm at the point where load was peaking. The load versus displacement curves of the loading - unloading DCB test (Fig. 4.32), show a non-negligible amount of permanent deformation. This could be attributed either as failure of the arms of the specimen in flexure or as indication of stitches' influence. The first hypothesis was not the case, since after examination of the failed specimens with C-Scan, no matrix failure was detected. This phenomenon has also been reported in previous studies [31], and it is explained as "pull-out" behaviour of the stitches. In particular, most of the stitches do not break in the plane of crack, but they tend to debond and then fail at the thread intersection at the outer surface of the composite. Hence, the stitches are pulled out but do not return to their original position during unloading and therefore undergo compressive stresses, leading to non-linear

unloading behaviour.

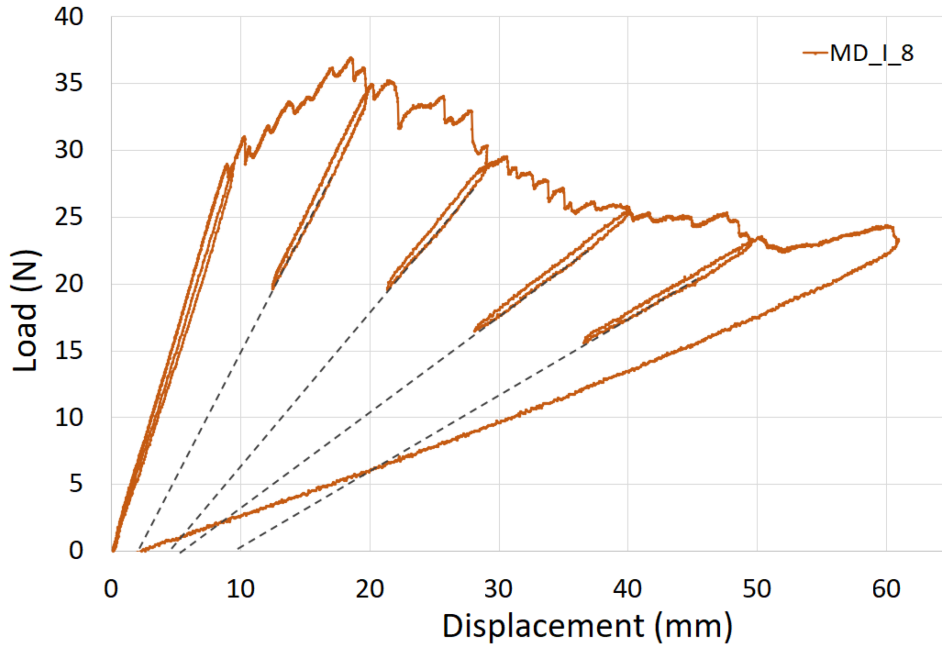


Figure 4.32: *Load - Displacement curve for loading - unloading Mode I DCB test of specimen MD I\_8.*

During testing no compressive failure on the outer face of the arms was observed as well as no warping noticed after the end of the tests. ASTM D5528 standard suggests three different reduction methods for calculating the interlaminar fracture toughness,  $G_{IC}$ : the Modified Beam Theory (MBT) method, the Modified Compliance Calibration Method (MCCM) and the Compliance Calibration (CC) method. Since it was unclear which data reduction method to apply, it has been decided by the author to proceed with a comparative study between the two most commonly used data reduction methods, namely MBT and MCCM. Hence, for all specimens the critical strain energy release rate was calculated both with MBT and MCCM, and the outcomes were compared with each other. The MBT method accounts the bending within the arms by using a crack length correction ( $\Delta$ ) and included also the rotation about the crack tip, leading to the following equation for the critical energy release rate [165]:

$$G_{IC} = \frac{3 \cdot P \cdot \delta}{2 \cdot B \cdot (\alpha + \Delta)} \quad (4.2)$$

where,  $P$  is the applied force (N),  $\delta$  is the crack opening (mm),  $B$  is the specimen width (mm)

and  $\Delta$  is the crack length correction, which is calculated as the negative intercept of the cube root curve against the crack length axis of Fig. 4.33. The MCCM generates a least squares plot of the delamination length normalised by specimen thickness,  $\alpha/h$ , as a function of the cubic root of compliance and it is expressed by the following formula [165]:

$$G_{IC} = \frac{3 \cdot P^2 \cdot C^{\frac{2}{3}}}{2 \cdot A_1 \cdot B \cdot h} \quad (4.3)$$

where,  $P$  is the applied force (N),  $C$  is the compliance (mm/N),  $B$  is the specimen width (mm),  $h$  is the specimen thickness (mm) and  $A_1$  is the slope of plot of Crack length correction. Figs. 4.34 and 4.33 present the compliance<sup>1/3</sup> versus crack length and the delamination length normalised by specimen thickness versus compliance<sup>1/3</sup> respectively for one of the tested specimens. The data reductions graphs for all specimens can be found in Appendix A.3 presented in Figs. A.2, A.3, A.4 and A.5. Figs. 4.35 and 4.36 summarise in the form of bar charts the results of the interlaminar fracture toughness calculated by both data reduction methods. Fig. 4.35 corresponds to the fracture toughness of crack initiation while Fig. 4.36 of propagation. For both data reduction methods, fracture toughness of crack initiation was determined through Eqs. 4.2 and 4.3 respectively, with the assistance of the Optical Immetrum camera, where at the moment that crack length was about to start propagating beyond 0 mm the corresponding load was recorded. In principle from both figures it is noted that the difference is minor and well below the 3% deviation threshold indicated by the ASTM D5528. Having a more detailed view on the results it is also noticeable that MBT method yielded the most conservative results of  $G_{IC}$  compared with to the MCCM.

The R-curve shown in Fig. 4.37 demonstrates a distinct initiation, increasing with crack length and then plateauing which indicated clear initiation and propagation toughness. The results are interpreted as fracture toughness of initiation and propagation as it is required by the numerical modelling. Therefore, the propagation toughness values were calculated as an average of the data points beyond where the R-curves tended to plateau. The plots of the R-curves for all the specimens are presented in Appendix A.4 through Figs. A.6 and A.7.

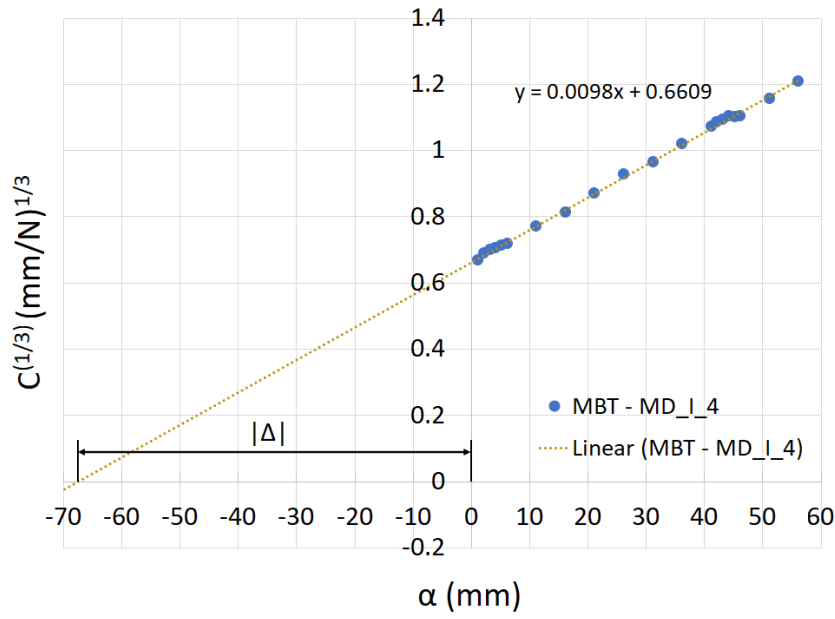


Figure 4.33: *Modified Beam Theory Method graph of specimen MD I\_4 for the DCB tests.*

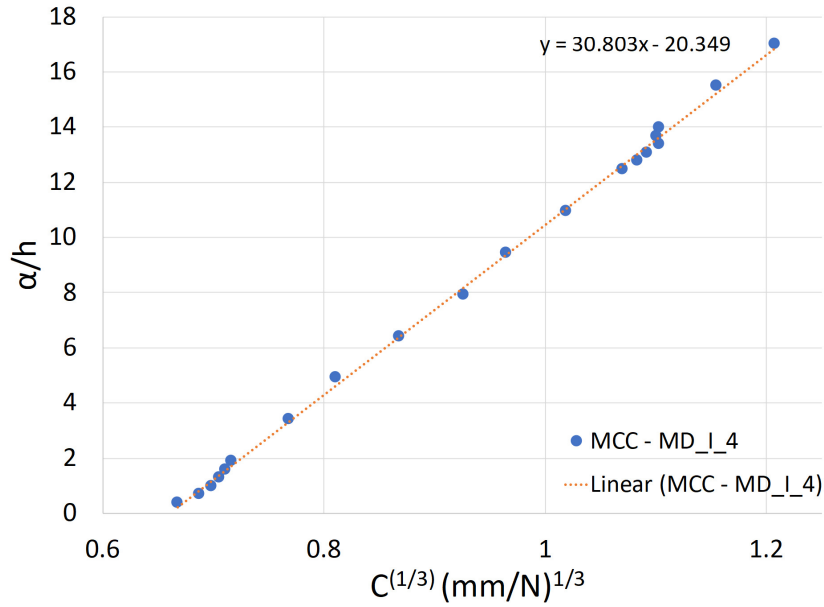


Figure 4.34: *Modified Compliance Calibration Method plot of specimen MD I\_4 for the DCB tests.*

Averaging from both methods, the values of  $G_{IC_{Init.}}$  and  $G_{IC_{Prop.}}$  are  $0.32 \text{ N/mm} \pm 3.2\% \text{ (CV)}$  and  $0.71 \text{ N/mm} \pm 4.4\% \text{ (CV)}$  respectively.

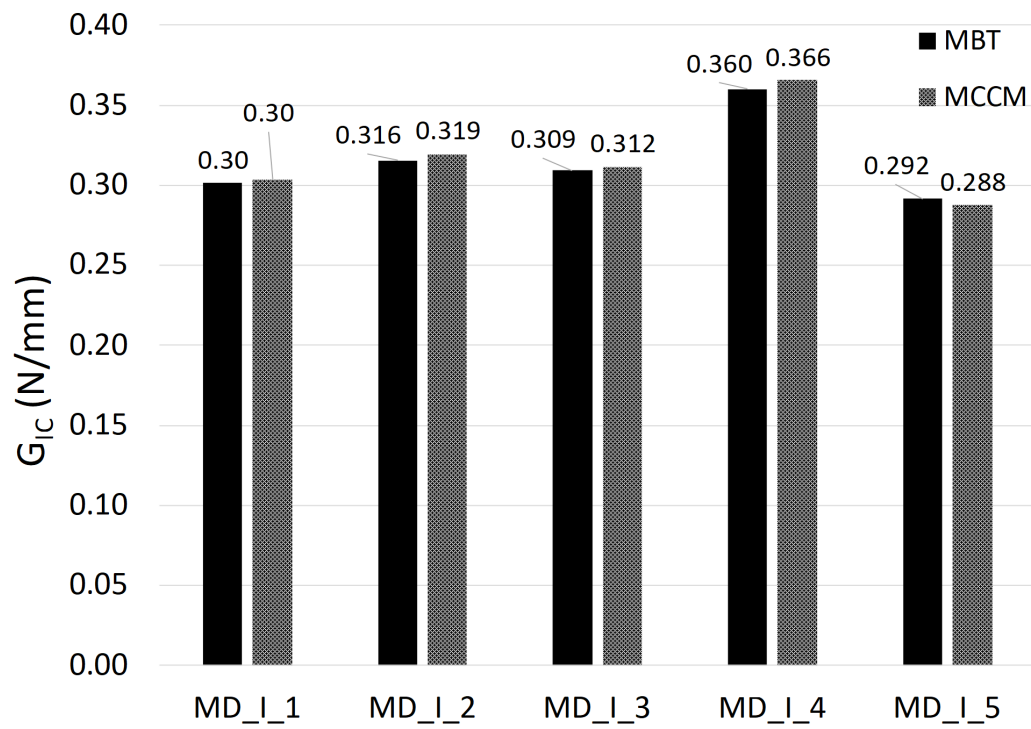


Figure 4.35: Interlaminar fracture toughness in damage initiation obtained from MCCM and MBTM for DCB tests.

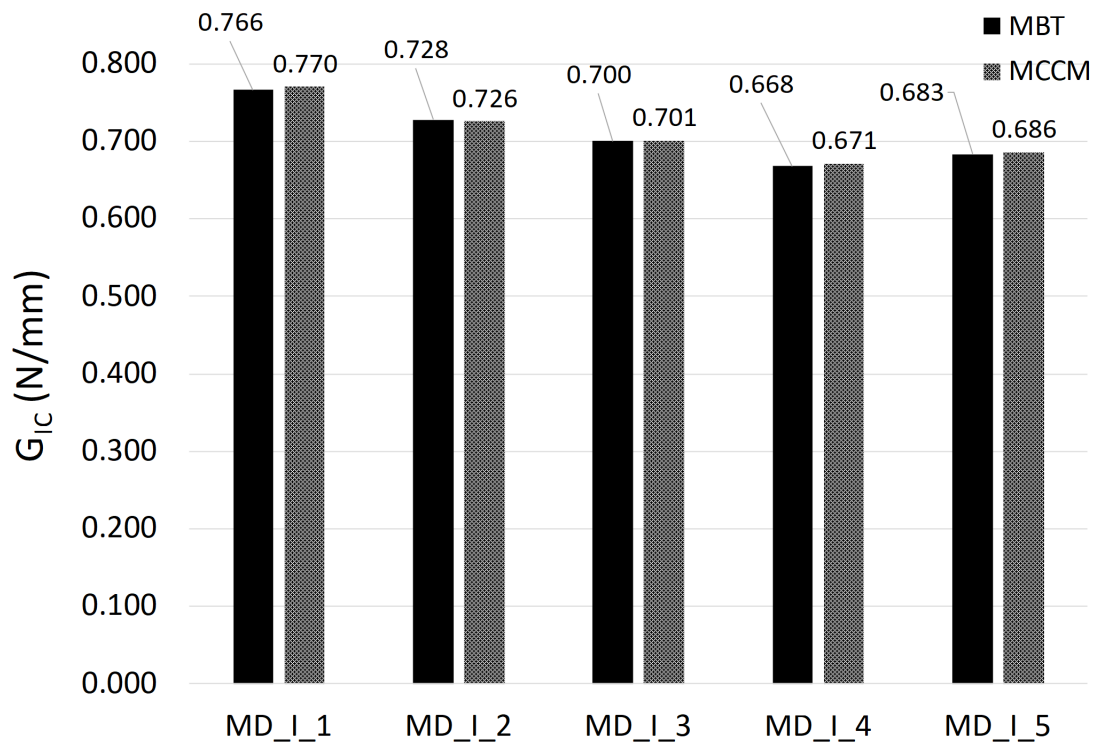


Figure 4.36: Interlaminar fracture toughness in damage propagation obtained from MCCM and MBTM for DCB tests.

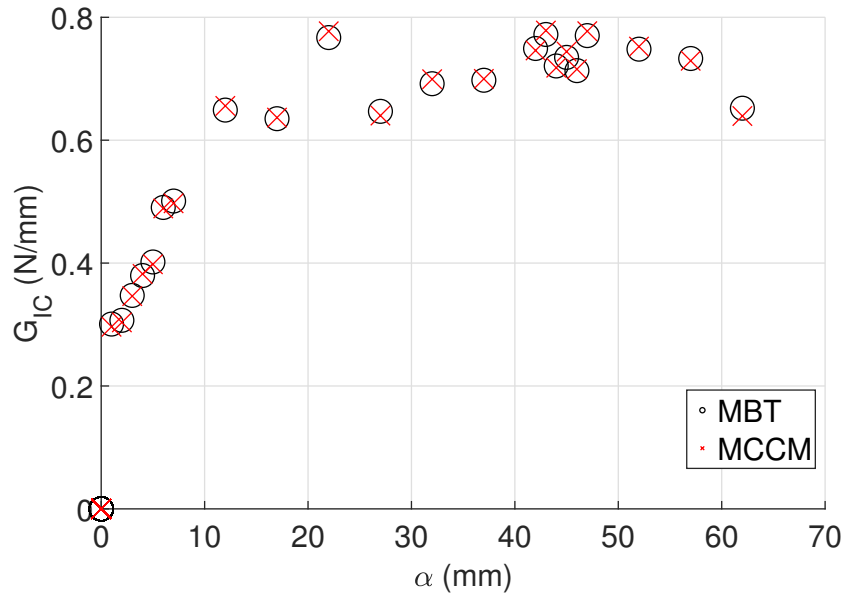


Figure 4.37: *Resistance (R) curves of specimen MD I.1 in DCB tests, expressing the interlaminar fracture toughness versus crack length, calculated both with MBTM and MCCM.*

#### 4.6.4 Fractography

It has been widely reported, as for example in [31], that delamination in NCF composites is not a dominant failure mechanism mainly because of the stitches which tend to enhance the Mode I delamination resistance. Typical fracture surfaces of a Mode I loaded NCF composite are presented in Fig. 4.38 (left). It is further obvious that the stitches do advance crimp development on the tows which although results in an undulating fracture surface, it increases the fracture toughness and exhibits a slip-stick fracture process [157]. At microscopic level the fracture extends along the fibres and then spreads into the surrounding matrix. Fibre splitting was also detected in Fig. 4.38 (left) through SEM in areas with high degree of crimp. Still though, this mechanism enhances fracture toughness. The white arrows indicate the direction of crack opening. In Fig. 4.39 (left and right) resin rich regions are illustrated, from which growth directions can be identified. These regions are associated with local crack initiation at the stitch and they are highly dependant on stitching parameters (i.e. stitch pitch, gauge, high/low stitch tension). Riverlines extend from fibres and converge with each other within the matrix adjacent to the fibres. These riverlines have a component of growth in the same direction with the global crack opening [166].

Furthermore, fractography revealed that stitches do play a significant role in constraining the blankets in the event of out-of-plane loading. Debonding of the stitch/matrix interface allowed elastic stretching of the stitches. This creates fibre bridging of the crack by the stitch lines [30], providing resistance forces in crack opening until stitch failure and pull-out occur (Fig. 4.38 (right)). Stitches bridging contributes in reducing the delamination extent and make the NCF composites more impact resistant and capable to absorb more energy.

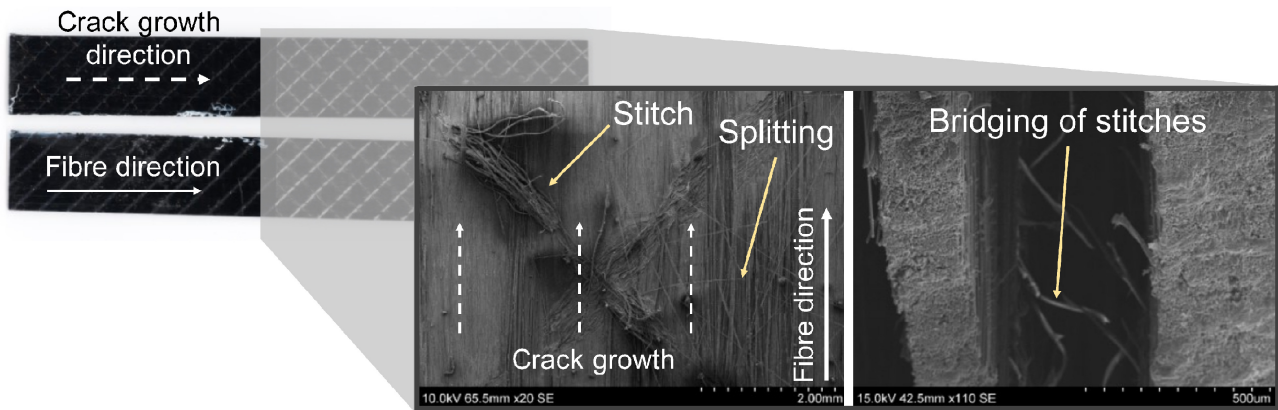


Figure 4.38: Mode I failed specimen: Intralaminar failure in a high crimp area (left, magnification:  $\times 20$ ), delamination and stitch bridging at fracture plane (right, magnification:  $\times 110$ ).

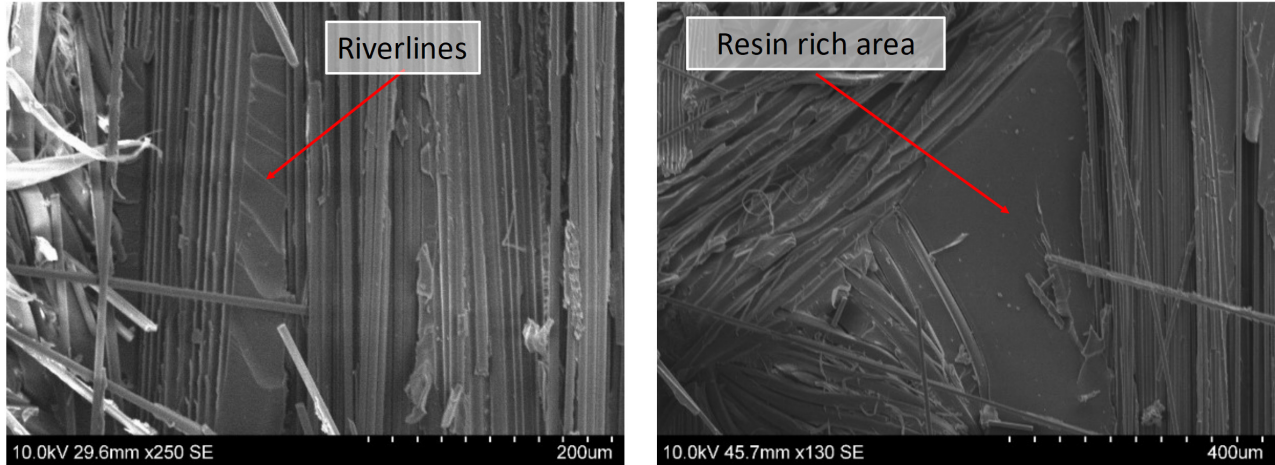


Figure 4.39: Mode I (DCB) failed specimen: Resin rich area exhibiting typical mode I fracture (left, magnification:  $\times 250$ , right, magnification:  $\times 130$ ).

## 4.7 Mode II - 4-point End Notched Flexure Testing

Mode II interlaminar testing utilised the non-standard 'Martin and Davidson' [140,141] method. The reason for opting this method regards the fact, that unlike the three-point bend end-notched

flexure (ENF), a stable crack growth under displacement control can be achieved [141]. Further research about 4pt-ENF method has been carried out more recently with main focus on the effects of friction and fixture compliance of 3- and 4-pt ENF [167, 168]. The research results revealed a larger Mode II toughness, by the 4pt-ENF than the 3pt-ENF test, due to the friction between the loading pins and the specimen. However, in the present experimental study no friction effects have been observed.

### 4.7.1 Specimen Preparation

Like the DCB specimens, the 4pt-ENF specimens were formed by a  $[90^\circ/0^\circ]_{7S}$  lay-up, with the  $0^\circ$  layers aligned to the long dimension of the specimen and situated at its midplane. The average dimensions of the Mode II 4pt ENF specimens are presented in Fig. 4.40, whereas in Tab. A.10 of Appendix A.2 the exact dimensions of all 4pt-ENF specimens are summarised. One side of the Mode II specimens was white painted and incremented at 1 mm in order to monitor the crack growth. The PTFE film was placed approximately 50 mm inside the midsection of the laminate while in terms of width, the insert was equally wide to the laminate. In the same way with Mode I specimens, the exact position of the PTFE film insert was recorded with the assistance of a stereoscopic microscope.

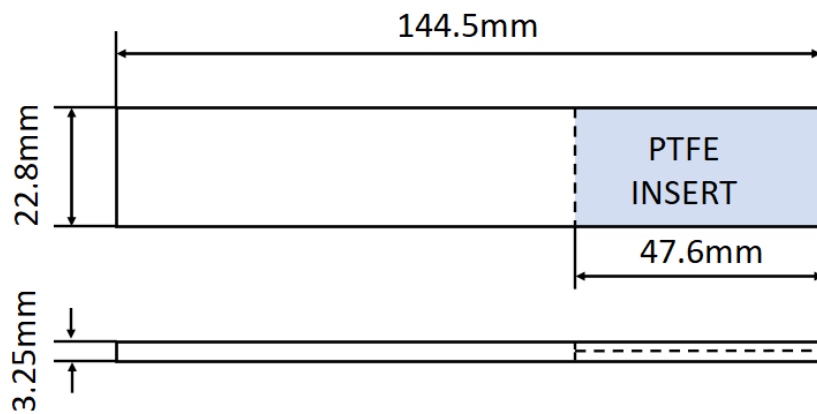


Figure 4.40: Average dimensions of the Mode II (4pt-ENF) specimens.



### 4.7.2 Test Setup

The tests were performed in an Instron 5960 test machine of 50 kN capacity. The 4pt-ENF test was set-up as shown in Fig. 4.41, where the support pins had nominal diameter of 10 mm and the support span equals 120 mm. Respectively, loading pins of 6 mm nominal diameter were set to a separation of 60 mm. The loading pins were free to rotate about a central pin across the specimen width. Like in the DCB case, the 4pt-ENF specimens were also pre-cracked at a stroke of 1 mm/min to a known crack length and then returned to the origin. Then the test was loaded in compression at a rate of 1 mm/min so that the stick-slip behaviour of the crack propagation can be monitored successfully by the Immetrum optical strain camera and then correlated to the force versus displacement data captured from the test machine.

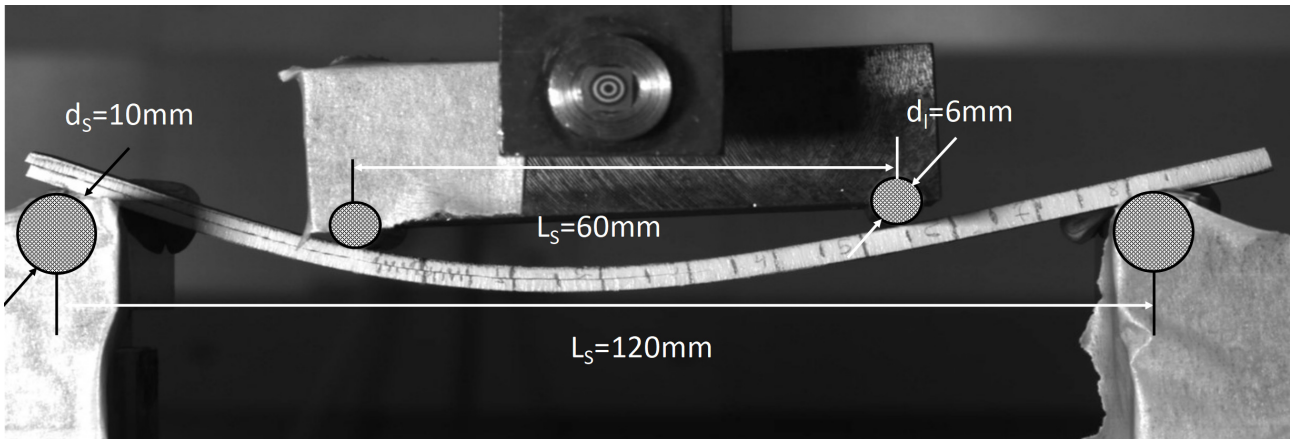


Figure 4.41: *Test set-up for 4pt-ENF.*

The Immetrum optical strain camera was set at a frequency as low as 3 Hz and together with the Instron test machine were correlated automatically via an analogue to digital converter thus the load from the cross-head and the optical displacement curves could be plotted purely from the pin location, minimising the test machine and instrumentation compliance errors.

### 4.7.3 Test Results

The typical load versus displacement curves obtained from the tests are demonstrated in Fig: 4.42. Some stick-slip growth can be observed particularly on the first increment, where there is an artificially high toughness because of the blunt delamination tip of the resin [140]. Once the

delamination propagates, the load tends to remain relatively constant with delamination length, which is a sign of a stable crack propagation. The initial increase in the crack propagation load with crack growth was mainly due to the development of bridging stitch thread zone. This results to the increase of the delamination toughness and hence a rising R-curve. Interestingly, some unstable crack propagation is also observed, leading to a sudden drop and the crack arrest at the next stitch line. All specimens seemed to be consistent and to exhibit very similar mechanical behaviour.

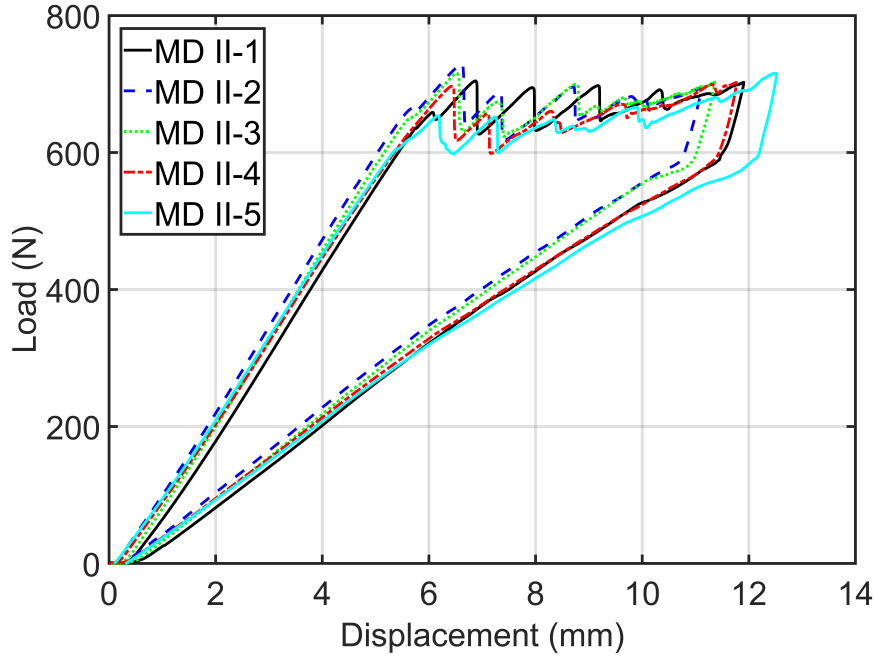


Figure 4.42: *Load - Displacement curves for the Mode II specimens in 4pt-ENF tests.*

The data reduction method implemented in 4pt-ENF test, is the Martin and Davidson Compliance Calibration method [140], which calculates the Mode II fracture toughness according to the following formula:

$$G_{IIC} = \frac{P^2}{2 \cdot B} \cdot m \quad (4.4)$$

where,  $P$  is the applied force (N),  $B$  is the specimen width (mm) and  $m$  is the linear gradient of the compliance with respect to the crack length ( $1/N$ ). Fig. 4.43 presents the Mode II interlaminar compliance versus crack length plot for specimen MD II\_3, whilst for the same specimen the resistance curve (R-curve) is depicted in Fig. 4.44.

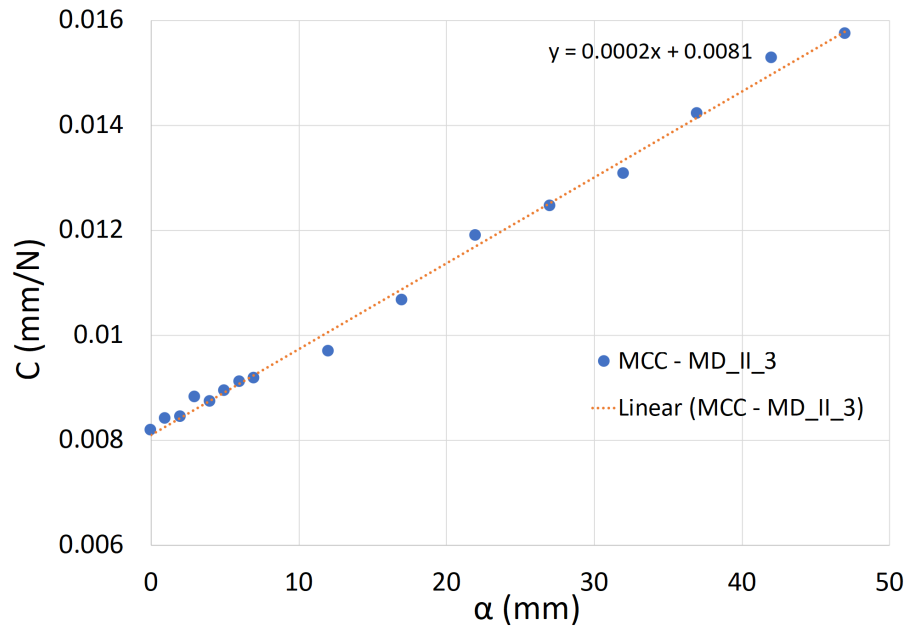


Figure 4.43: *Martin and Davidson Compliance Calibration Method graph of specimen MD II\_3 for the 4pt-ENF test.*

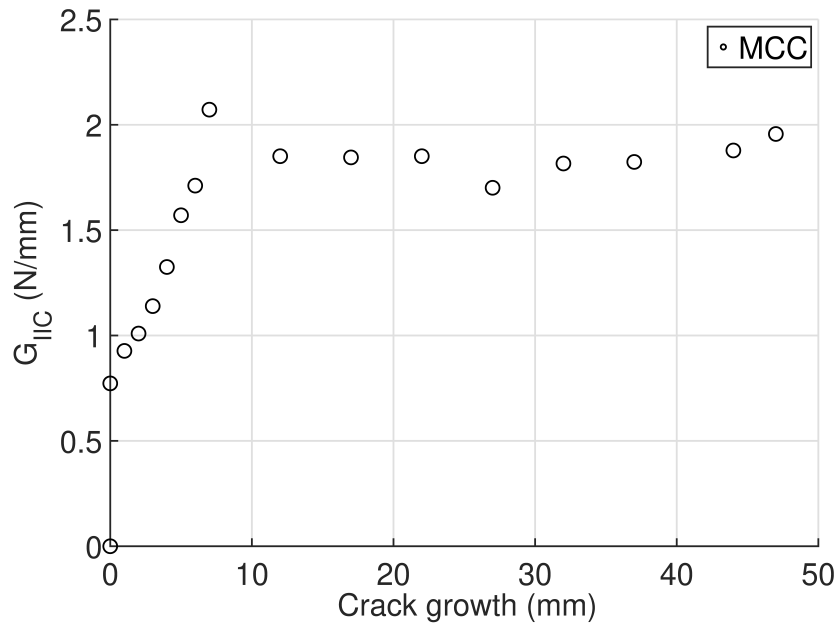


Figure 4.44: *Resistance (R) curves of specimen MD II\_1 in 4pt-ENF tests, expressing the interlaminar fracture toughness versus crack length.*

The compliance calibration graphs as well as the R-curves for the all specimens can be found in Appendices A.5 and A.6 and specifically in Figs. A.8, A.9 and Figs. A.10, A.11 respectively. Fig. 4.46 summarises the initiation and propagation values respectively for all specimens as obtained from the R-curves. Due to the numerical model requiring an initiation and propagation toughness value, the average of the data points upon a reasonably acceptable plateau of the

R-curves was utilised for a propagation toughness value. The obtained average values of  $G_{IIC_{Init.}}$  and  $G_{IIC_{Prop.}}$  are  $0.693 \text{ N/mm} \pm 2.5\% \text{ (CV)}$  and  $1.67 \text{ N/mm} \pm 4.2\% \text{ (CV)}$  respectively.

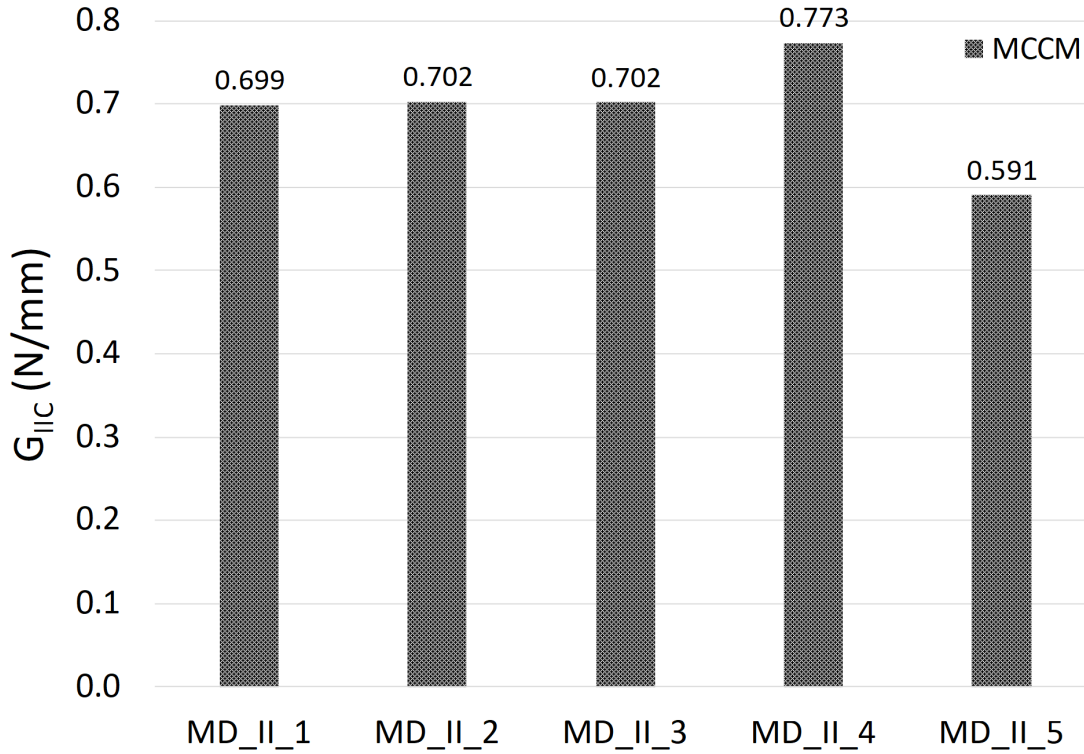


Figure 4.45: Interlaminar fracture toughness in damage initiation obtained from MCCM for 4pt-ENF tests.

#### 4.7.4 Fractography

Stitches again have a significant impact in fracture toughness enhancement. At the resin rich areas associated with the stitches large cusps are often apparent. Stitches, as observed in Fig. 4.47 (left and right), can promote ply splitting (intralaminar failure) which interacts with Mode II fracture, enhancing the toughness and modifying the crack path [65].

It was very difficult to determine the crack growth direction from visual observation of the interlaminar shear fracture surfaces, nevertheless with the assistance of SEM, the presence of surface debris gave an indication of sequence as pointed by the white arrows (Fig. 4.48 (left and right)). Figure 4.48 (right) reveals the formation of cusps, which appear as inclined platelets on the surface. The large number of small cusps indicates that the fibres are packed closely to

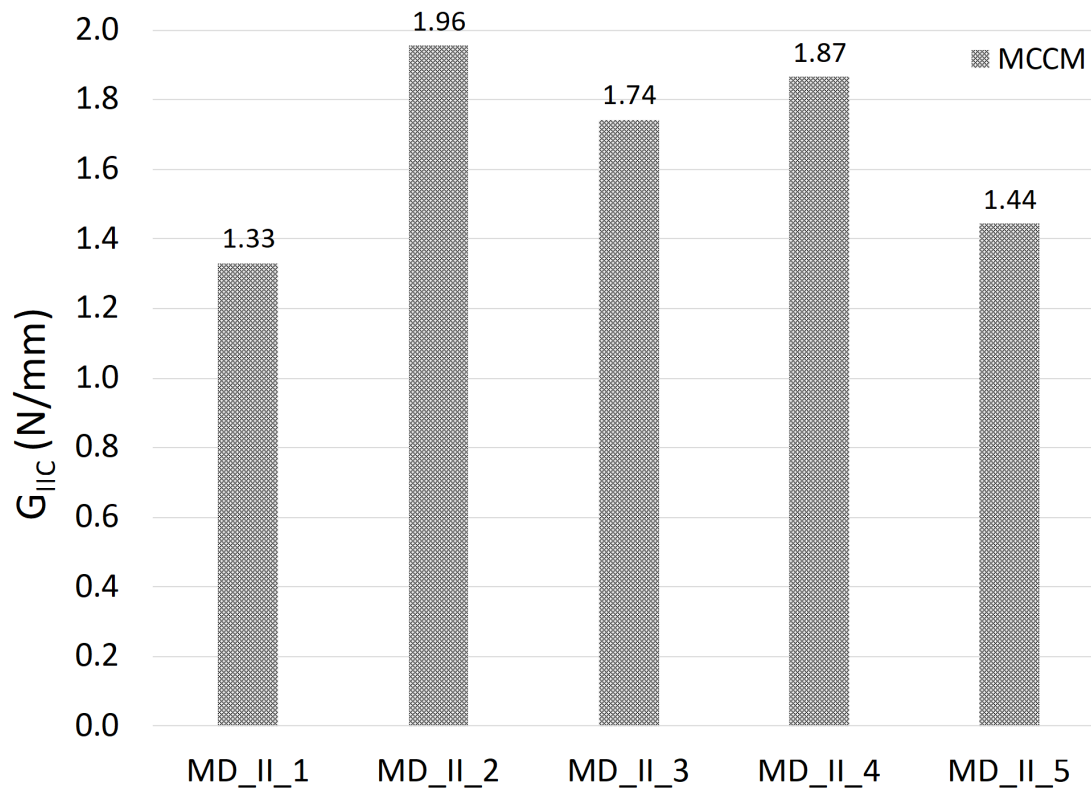


Figure 4.46: Interlaminar fracture toughness in damage propagation obtained from MCCM for 4pt- ENF tests.

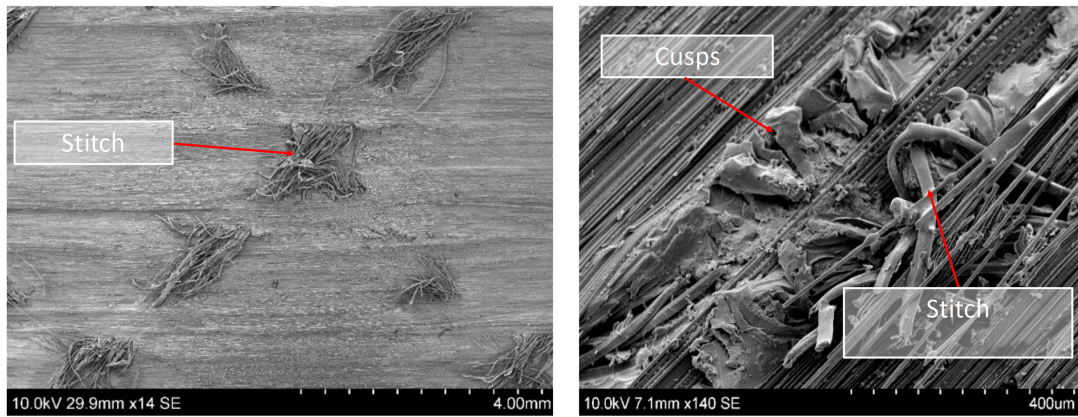


Figure 4.47: Transverse splitting and stitch ploughing (left, magnification:  $\times 14$ ), Large cusps with stitches and fibre splitting (right, magnification:  $\times 140$ )

each other [169], hence correspond to high fibre volume fraction ( $>60\%$ ). The matrix fracture initiates at the fibre/matrix debonding and then extends as scarps into the matrix. Cusps are adjacent to the fibre plane, which results that one surface of the specimen half retains the cusps, while the other contains only scallops (Fig. 4.48 (left)).



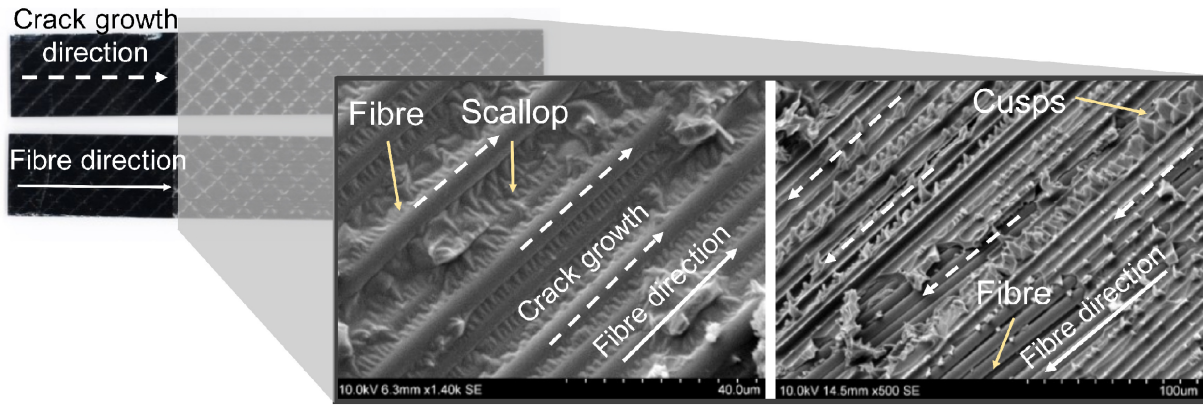


Figure 4.48: *Mode II (4pt-ENF) failed specimen: Scanned fracture surfaces: Scallop features (left, magnification:  $\times 1.4k$ ) and shear cusps (right, magnification:  $\times 500$ ).*

## 4.8 Compact Tension Testing

The translaminar fracture toughness is an essential material parameter when modelling impact and damage propagation of fibre dominated failures. Laffan et al. [170] presented a comprehensive review of the various test techniques and data reduction methods through which the translaminar fracture toughness could be obtained. Several researchers [73, 79, 170–172] have characterised the fracture toughness of CFRP composites using the CT specimen design, however they reported that the success of the test is significantly dependant upon the relatively similar tensile and compressive strength of the material systems. Blanco and Pinho [75] presented the crucial areas where unwanted failure might occur in a typical CT specimen (Fig. 4.49). They named the invalid failure modes in the following form: FM1/FM2: local compression, FM3: in-plane shear, FM4: bearing, FM5: shear-out and FM6: out-of-plane buckling.

Compressive stresses acting at the un-notched rear side of the specimen result in premature compressive failure (FM1) in that area of the specimen, the most common and significant undesired failure that occurs while opening the CT arms. This failure mode prohibits the determination of the actual translaminar fracture toughness in tension while these stresses are also responsible for out-of-plane buckling [173]. Research studies [77, 78] have shown that high crimp and the low compressive strength compared to tensile strength play a significant role in the development of premature compressive failure. Numerous studies have been devoted to

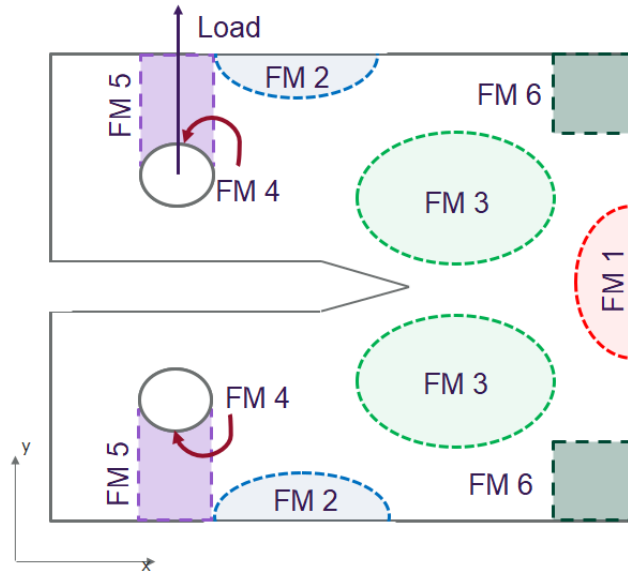


Figure 4.49: *Conventional CT specimen depicting the areas of possible failure mechanisms [1].*

improve the CT specimen geometry, either by investigating the thickness [171], the area [174] and the critical notch tip radius [77] of the specimen and correlate it with the translamellar fracture toughness. In the recent years, numerous attempts [75, 142, 171] have been made in developing novel specimens, which could potentially prevent the premature compressive failure of the specimens during CT test. However, the author opted to follow the geometry proposed by Pinho et al. [73] (Fig. 4.49). The reasons for selecting this geometry regarded the previous application of this geometry in NCF composites [79] and the reduced labour time for specimen manufacturing.

#### 4.8.1 Specimen thickness definition - Eigenvalue analysis

The lay-up of the specimen is  $[90^\circ/0^\circ]_{ns}$ , where  $n$  denotes the number of layers and  $0^\circ$  fibres are parallel to the load direction in order to obtain larger energy consumption than any matrix failure. The thickness of specimen and hence the number of layers are defined through a numerical eigenvalue buckling analysis as suggested by Katafiasz & Iannucci [142], which also determines the critical buckling load for a given thickness of the specimen. Hence, after an iterative procedure the optimal thickness of the specimen was determined in such way so that the probability in experiencing premature compressive failure (Fig. 4.49, FM 1) during CT test is minimised. The crack is expected to initiate on the middle of the specimen due to

geometric and load symmetries whereas the rear part of the specimen is the most critical area to experience out-of-plane buckling (Fig. 4.49, FM 6).

Using the selected CT specimen geometry (Fig. 4.54), the eigenvalue buckling analysis was carried out in Abaqus 2017, applying continuum shell elements (SC8R) to model the specimen and tie constraints to replicate the pins. The continuum shell elements were assigned the material properties obtained through the previously presented mechanical tests and also assigned the composite section properties for each NCF blanket. Each NCF blanket was modelled with 6 integration points, 3 for each layer ( $0^\circ$  and  $90^\circ$ ). A mesh sensitivity analysis was employed and discrete element sizes were selected. In particular, around the notch tip at a radius of 3 mm the size of the element was finally 0.1 mm selected, while the globally assigned size of the elements was 1.5 mm, resulting in a CT model of 32240 elements and 47635 nodes. After running multiple models of specimen thickness varying from 6 mm to 8 mm, at discrete crack lengths (0, 5, 10 and 15 mm) (Fig. 4.50) and identifying the out-of-plane buckling as out-of-plane displacements, the critical buckling loads,  $P_C$  are determined. (Table 4.7).

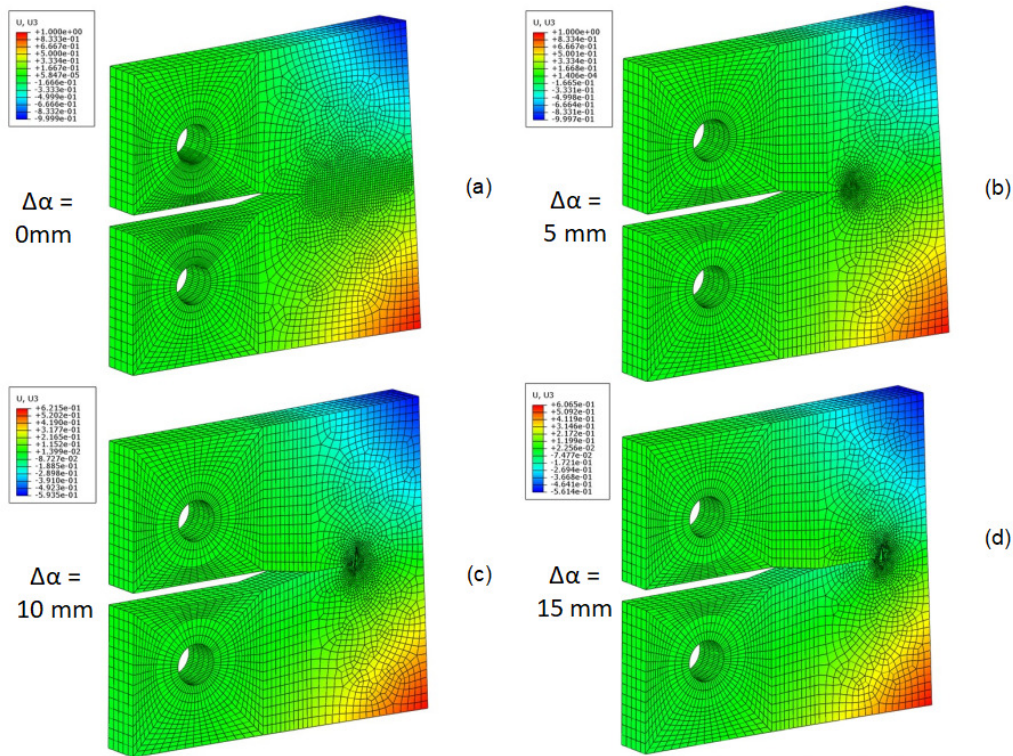


Figure 4.50: First eigenvalue buckling mode, out-of-plane displacement for crack length a) 0 mm, b) 5 mm, c) 10 mm and d) 15 mm.



However, complementary to the eigenvalue buckling analysis, for each of the discrete crack lengths, a linear static FE analysis utilizing the maximum stress criterion was performed so that the minimum required load for crack evolution ( $P_T$ ) to be determined and to examine whether the condition  $P_T < P_C$  is met for all crack lengths. The value of fibre tensile strength,  $X_T$  at ply level which defines the denominator of the maximum stress criterion was obtained from the tensile tests presented in Subsection 4.3 by using the reverse ROM and found to be equal to 2485 MPa. From the linear static FE analyses it was observed that for all discrete crack lengths the maximum stress criterion was activated ( $= 1$ ) (Fig. 4.51), without activating other undesired failure modes.

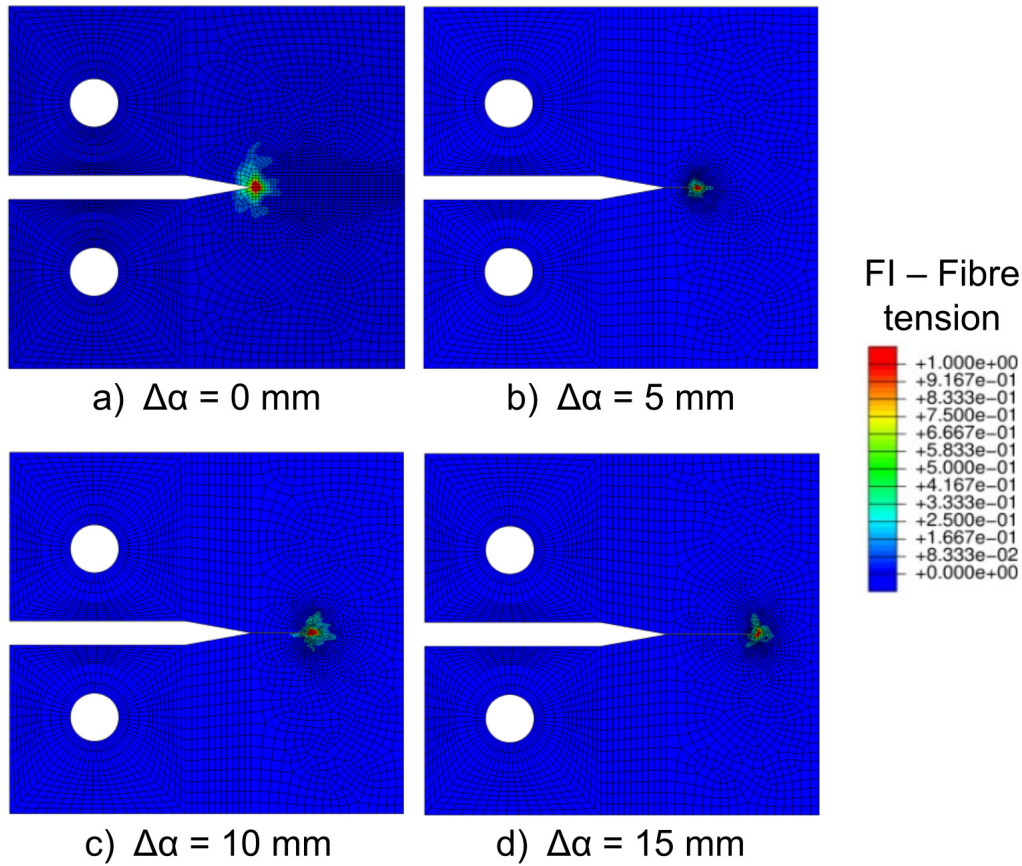


Figure 4.51: *Maximum stress criterion illustration for crack lengths a) 0 mm, b) 5 mm, c) 10 mm and d) 15 mm. The red contour indicates failure initiation.*

Table 4.7 summarises the values of the critical buckling and the translaminar fracture loads at discrete crack lengths of a 7.8 mm thick CT specimen, which was found to be the optimal thickness after following an iterative procedure. It is apparent that the failure loads are significantly lower than the critical buckling loads, meaning that crack onset and propagation can

occur before buckling.

| Crack length, $\alpha$ (mm) | $P_C$ (kN) | $P_T$ (kN) |
|-----------------------------|------------|------------|
| 0                           | 45.4       | 10.1       |
| 5                           | 28.5       | 6.0        |
| 10                          | 9.24       | 3.1        |
| 15                          | 4.1        | 2.3        |

Table 4.7: Critical buckling load,  $P_C$  and translaminar damage load,  $P_T$  per crack length of a 7.8 mm thick specimen.

Having the optimal thickness of the specimen (7.8 mm) determined, the thickness of the laminate (namely laminate RFL5 of Chapter 3) is also defined and a  $[90^\circ/0^\circ]_{14S}$  lay-up is formed. The final dimensions of the specimen are depicted in Fig. 4.52, whereas in Tab. A.11 of Appendix A.2 the dimensions of all specimens are summarised.

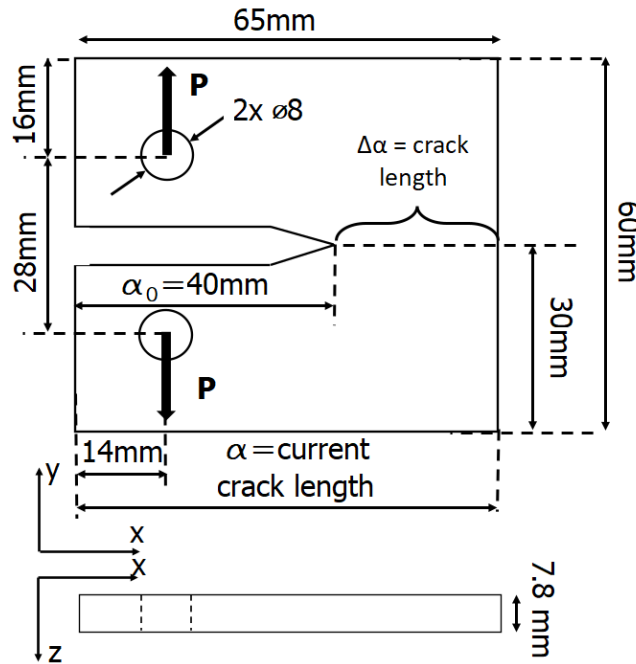


Figure 4.52: Average dimensions of a CT specimen.

### 4.8.2 Specimen Preparation

The specimens were water-jet cut from the laminate and C-scanned to identify potential damage (Fig. 4.53). The notches of the specimens are primarily machined using a diamond coated disk saw and then cut by hand with a surgical scalpel to guarantee an accurate and sharp crack tip [77]. Moisture removal techniques were followed for all specimens according to ASTM

D5229 [154].

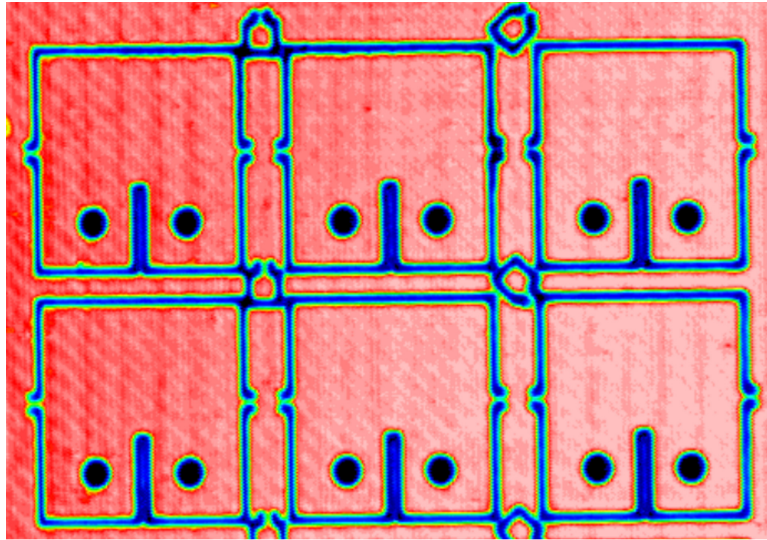


Figure 4.53: *C-Scan image of the waterjet-cut CT specimens from RFL\_5 laminate.*

One side of the each CT specimen is speckle patterned with white spray paint to create a proper mapping for the DIC analysis. The GOM DIC camera with maximum resolution 4096 pixels x 3000 pixels was used to measure the axial and transverse strain and displacement fields and detect any out-of plane bending effects of the specimens, while GOM Correlate software was utilised to postprocess the test results. The Region Of Interest (ROI) was the area defined in Fig.4.54 (60 mm x 40 mm - 2900 pixels x 500 pixels). The pixel resolution was 0.05 mm whereas the subset size was set to 19 pixels x 19 pixels and point distance of 16 pixels was used meaning a 50% overlap, as suggested by Aramis for 6 Megapixel CCD cameras. To filter undesired data, a simple mean filter was used in the DIC commercial software Aramis GOM setting value 17. On the other side of the specimen a scale incrementation with accuracy of a millimetre is created to facilitate the view of the crack growth using Immetrum the optical strain camera (Fig. 4.54). The DIC, the Immetrum optical strain and the Instron test machine sources are correlated automatically at a 5 Hz frequency via an analogue to digital converter so that the load from the cross-head and the optical displacement curves to be plotted purely from the pin locations, eliminating possible compliance errors from the test machine and the instrumentation. All previously described parameters of the devices were verified after performing sample tests and compared against already validated data.

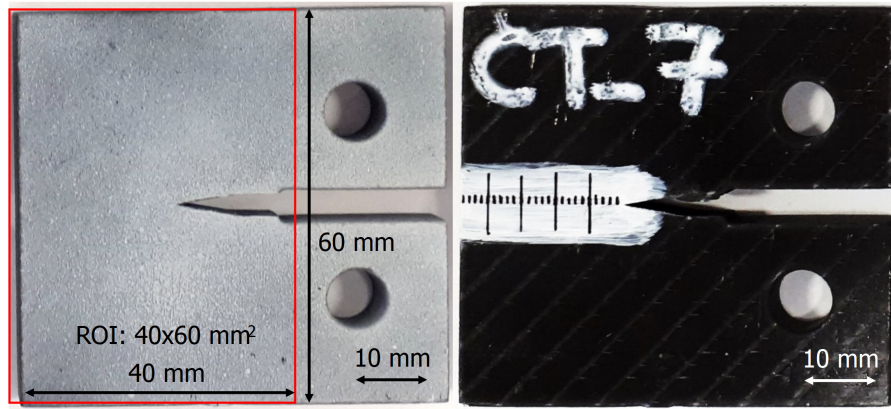


Figure 4.54: *Illustration of both sides of the CT specimen (scale 2:3).*

### 4.8.3 Test Results

The CT tests followed the test method suggested by Pinho et al. [73] and were carried out using an Instron 5969 test machine at a constant tensile rate of 1mm/min, where the displacement (mm), load (N) and time (sec) were recorded. The DIC, Immetrum optical strain and the Instron test machine sources were correlated automatically via an analogue to digital converter and the load from the cross-head and the optical displacement curves could be plotted purely from the pin locations, eliminating the test machine and instrumentation compliance errors. Five specimens were tested and the produced load-displacement curves are shown in Fig. 4.55. The initial failure was identified as the first significant load drop in the load-displacement curve, whereas the compressive failure at the rear end of the specimen occurred in the grey-scaled highlighted load drops for each specimen. Thus, the load response after that point has been disregarded from the calculations.

To calculate the critical strain energy release rates for the tested specimens the use of a data reduction method is required. Several data reduction methods have been presented in the literature. Such methods are the Area Method [175], the critical stress intensity factor,  $K_{IC}$  method [176] and the Compliance Calibration (CC) method [177,178]. All three methods require measurement of the crack length which is heavily dependant on user's precision to record the load reading and the corresponding crack length. Hence these methods are sensitive to errors. More specifically, the  $K_{IC}$  method was originally developed for metals and it has been reported [73,179] that is less accurate if the anisotropic nature of composites is not accounted.

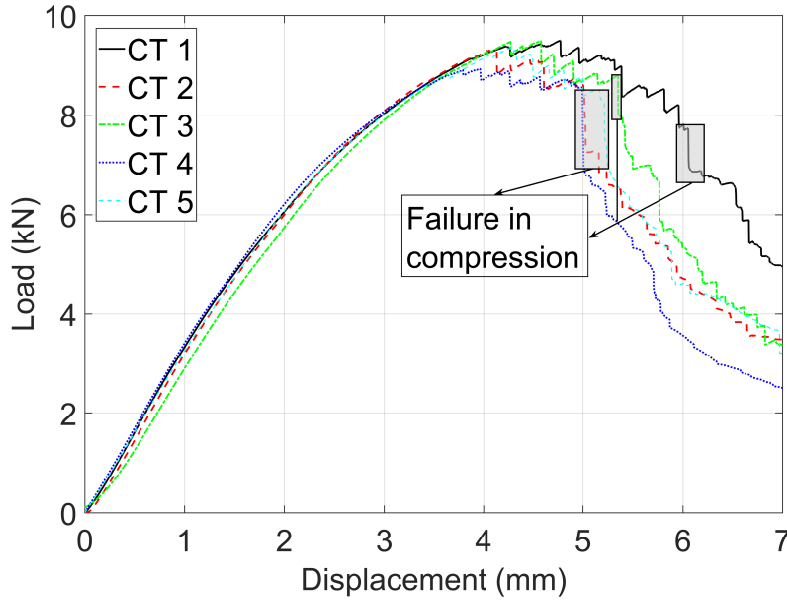


Figure 4.55: Load - Displacement curves of CT specimens in compact tension tests.

With the assistance of the DIC to obtain full field measurements of the CT specimen, Catalanotti et al. [74] developed a method which couples with the J-integral method and determines the R-curves successfully [77]. In this work, however, the MCCM [179] is selected, a simple yet accurate method [179]. The relation between the compliance and the crack length is described by Eq. 4.5 [74, 180]:

$$C = C_0 + (\chi \cdot \Delta a + \beta)^\nu \quad (4.5)$$

where,  $C$  is the compliance (mm/N) and  $\chi$ ,  $\beta$  and  $\nu$  are curve fitting coefficients. The critical energy release rate of the laminate is calculated using Eq. 4.6 [178]:

$$G_{Lam} = \frac{P_c^2}{2 \cdot t} \cdot \frac{dC}{d\Delta a} \quad (4.6)$$

where:  $P_c$  is the critical load (kN) prior to crack growth,  $t$  is the thickness of the specimen in mm and  $dC/d\Delta a = \Delta a \cdot \chi \cdot (\Delta a \cdot \chi + \beta)^{(\nu-1)}$ , is the derivative of the compliance with respect to crack length. Fig. 4.56 presents the experimentally derived MCCM for specimen CT\_1, whilst the respective plots for the other tested CT specimens are included in A.7 (Appendix A) of the

present document in Figs. A.12 and A.13. Table 4.8 presents the coefficients of the MCCM for all specimens.

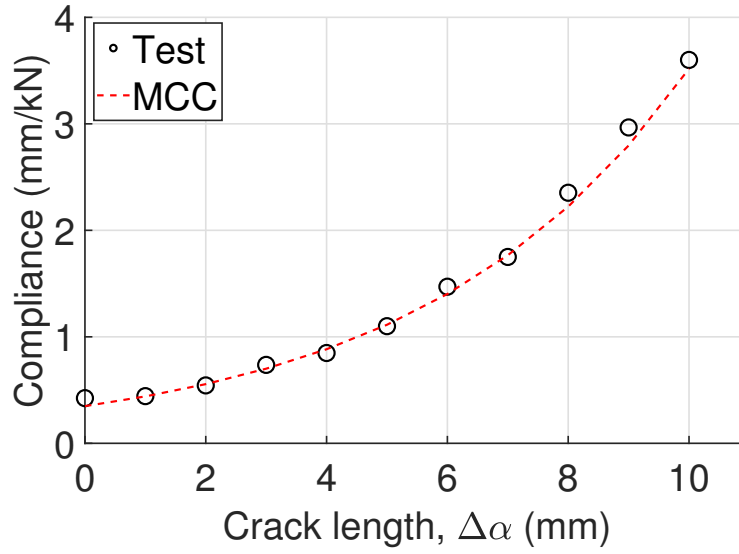


Figure 4.56: *Modified compliance calibration method graph of specimen CT\_1.*

| Coupon | $\chi$  | $\beta$  | $\nu$     |
|--------|---------|----------|-----------|
| CT_1   | 0.00162 | 0.9926   | 141.8289  |
| CT_2   | 0.00163 | 0.9926   | 141.8288  |
| CT_3   | 0.00163 | 0.99261  | 141.8288  |
| CT_4   | 0.00163 | 0.992606 | 141.8288  |
| CT_5   | 0.00163 | 0.99261  | 141.82886 |

Table 4.8: *Coefficients for the MCCM data reduction.*

The R-curves obtained for the CT specimen from the CT tests are shown in Fig. 4.57, where the initiation values of the translaminar fracture toughness of the laminate are defined as the intersection between the linearly increased dashed line and the vertical axis. The average fracture toughness obtained for initiation,  $\overline{G}_{Lam_{Init.}}$  is 42 N/mm with coefficient of variation (CV) of 5.67%. Although compressive failure occurred at 10 mm of crack length, some meaningful propagation values can still be obtained. The R-curves seem to plateau after 3 mm of crack length and the fracture toughness for propagation can be obtained. The horizontal dashed line defines the average fracture toughness of propagation,  $\overline{G}_{Lam_{Prop.}}$  and is equal to 65 N/mm (CV 6.76%).

With a simple use of the ROM the crack resistance behaviour of the  $0^\circ$  layers,  $G_{ICT}$  can be obtained from Eq 4.7 [171]:



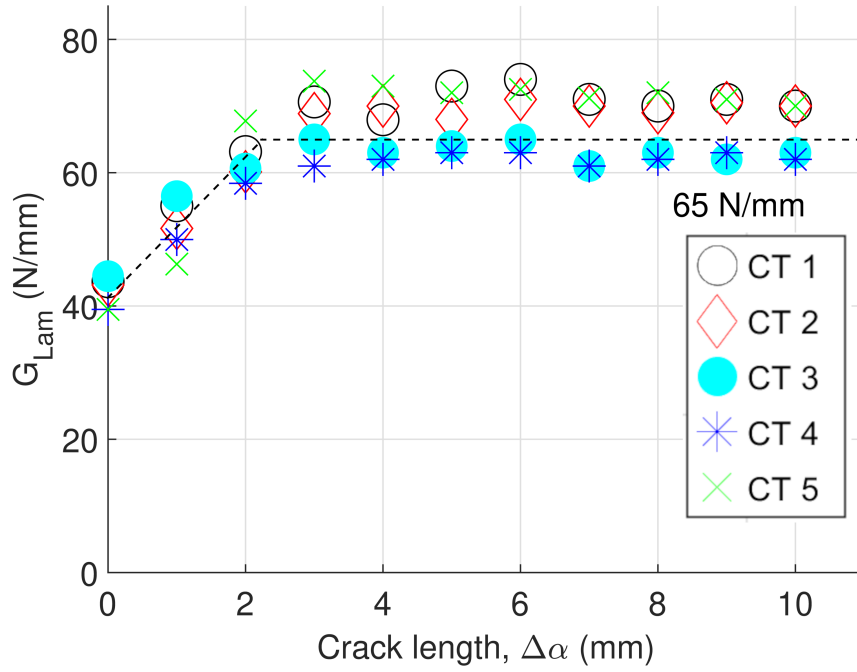


Figure 4.57: Translaminar fracture toughness of the CT specimen.

| Coupon | $G_{Lam_{Init.}}$<br>(N/mm) | $\bar{G}_{Lam_{Init.}}$<br>(N/mm) | CV<br>(%) | $G_{Lam_{Prop.}}$<br>(N/mm) | $\bar{G}_{Lam_{Init.}}$<br>(N/mm) | CV<br>(%) |
|--------|-----------------------------|-----------------------------------|-----------|-----------------------------|-----------------------------------|-----------|
| CT_1   | 44.3                        |                                   |           | 69                          |                                   |           |
| CT_2   | 43.7                        |                                   |           | 67.3                        |                                   |           |
| CT_3   | 44.1                        | 42.4                              | 5.67      | 61                          | 67.3                              | 6.76      |
| CT_4   | 39.8                        |                                   |           | 62                          |                                   |           |
| CT_5   | 39.9                        |                                   |           | 66                          |                                   |           |

Table 4.9: Translaminar fracture toughness values of initiation and propagation for CT tests.

$$G_{ICT}^{0^\circ} = \frac{G_{Lam}(\Delta a) \cdot t_{Lam} - G_{ICT}^{90^\circ} \cdot t_{90_T}}{t_{0_T}} \quad (4.7)$$

where,  $t_{Lam}$  is the thickness of the specimen and  $t_{90_T}$  and  $t_{0_T}$  are the aggregate thicknesses of the  $90^\circ$  and  $0^\circ$  layers respectively. From OM observations the average thickness of each  $90^\circ$  and  $0^\circ$  layer equals to 0.1393 mm. In Eq. 4.7,  $G_{Lam}(\Delta a)$  is expressed as a function of the crack length ( $\Delta a$ ), while  $G_{ICT}^{90^\circ}$  corresponds to the fracture toughness of the  $90^\circ$  layers. Although  $G_{ICT}^{90^\circ}$  is relatively low and is commonly considered negligible, in this work, as it was also presented in [181], it is taken equal to the interlaminar fracture toughness due to peel (Mode I),  $G_{IC}$  which is obtained through DCB tests. Fig. 4.58 shows the R-curves for the  $0^\circ$  layers, where the fracture initiation,  $G_{ICT_{Init.}}^{0^\circ}$  value is equal to 83 N/mm and the average fracture propagation,

$G_{ICT_{Prop.}}^{0^\circ}$  value is 134.5 N/mm. The specimen R-curves present the same trend and the fracture initiation values show good agreement.

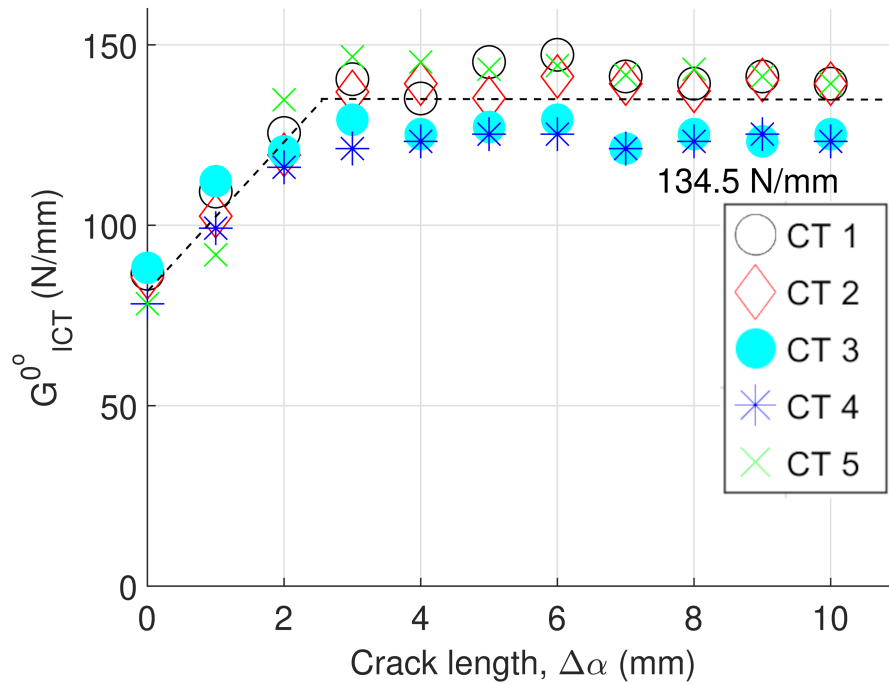


Figure 4.58: Translaminar fracture toughness of the  $0^\circ$  layers as obtained from Eq. 4.7.

#### 4.8.4 Fractography

The failure mechanisms detected during CT test could be summarised in the following sequence: (1)  $90^\circ$  layers splits form first, (2) followed by splitting of the misaligned  $0^\circ$  layers and (3) finally fibre fracture occurs. The side view and the crack opening of the CT specimen is shown in Fig. 4.59a), where with the utilisation of the SEM, images illustrating typical fracture surface are presented in Fig. 4.59b), c) and d). Using the SEM, images illustrating typical fracture surface are shown in Fig. 4.59. The fracture surface cannot be described as absolute planar since a significant amount of fibre pull-out in the  $0^\circ$  layers is exhibited (Fig. 4.59c)), which results in fibre bridging towards the direction of crack growth (as white arrows indicates - Fig. 4.59b)). The stabilisation of the fibre bridging is depicted in the R-curves graphs (Figs. 4.57 and 4.58). Another parameter which is observed, regards the height of the fibre pull-out which was identified to be higher at the mid-thickness of the specimen, where the  $0^\circ$  layers are blocked together, and it implies that more energy is dissipated during crack growth as it was also reported in [182].



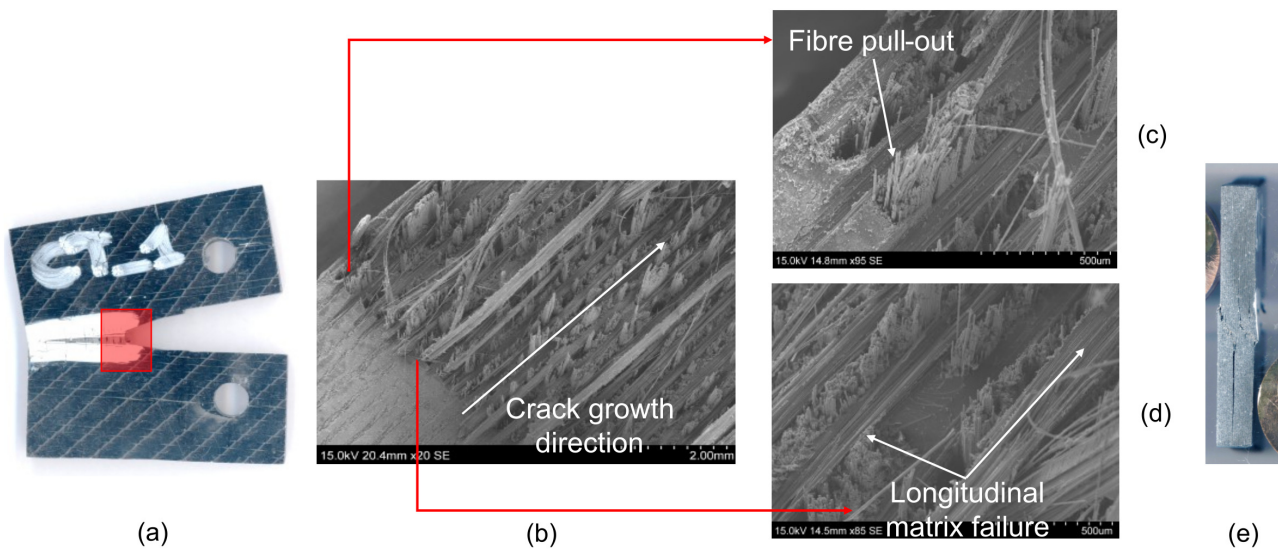


Figure 4.59: a) Side view of a failed CT specimen (scale 1:2), b) SEM graph of fracture surfaces on CT failed specimen (magnification x20), including magnification of c) fibre pull-out (x95) & d) longitudinal matrix failure (x85) and e) angled fracture due to compressive failure at the rear end of a CT specimen (scale 1:2).

In addition to the fibre failure, it has been identified that fibre splits and subsequent pull outs are formed [183]. The formation of splits and the energy dissipated by friction during pull out corresponds to a great portion of the energy dissipated during translaminar fracture toughness of NCF composites. The  $90^\circ$  layers are characterised by flat regions which are associated with matrix failure or fibre-matrix de-bonding (Fig. 4.59d)). In these surfaces riverlines are noticed as well, which denote that the fracture initiated from the  $90^\circ$  layers and then propagated towards the  $0^\circ$  layers. This phenomenon is also identified in previous studies [73, 174, 183]. The ultimate failure mode of the CT specimen is the compressive failure at the rear end of the specimen which is shown in Fig. 4.59e) as an angled fracture plane. Aside from the SEM inspection, the failed specimens are also C-scanned. The C-scan outcome as well as the strain maps from the DIC camera are presented in Chapter 8 of this thesis, where the inspection findings are compared with the numerical results.

# Chapter 5

## Constitutive Model

### 5.1 Introduction

The proposed constitutive model of this study is developed according to the experimental observations discussed in Chapters 3 and 4 of the thesis and is composed by four pillars (Fig. 5.1):

- Material properties
- Longitudinal stiffness calculation
- Failure criteria
- Energy-based Continuum Damage Mechanics method

At first place the material properties at layer level are calculated via a mesomechanical approach whose principal is to use the mechanical properties of the constituents (i.e., fibre, resin and stitches) in order to obtain the mechanical properties of the layer and limit the dependence on the properties extracted from the mechanical testing. A knock-down factor is then introduced to estimate the effective longitudinal stiffness of the layer, through which the in-plane and out-of-plane fibre undulation is accounted. Failure onset is triggered by a set of physically-based failure criteria, composed by four discrete failure modes, namely in fibre tension and compression and

matrix, which includes specific characteristics of the NCF composites. The above-mentioned analytical formulations are implemented in an energy-based Continuum Damage Mechanics layer-wise approach, originally developed by Iannucci [12,13,184] already capable of modelling UDPT and woven composites and through this study is further extended to model biaxial NCF composites. To the CDM model, a newly proposed exponential damage evolution law is proposed to simulate fibre failure together with an interactive matrix damage propagation formulation.

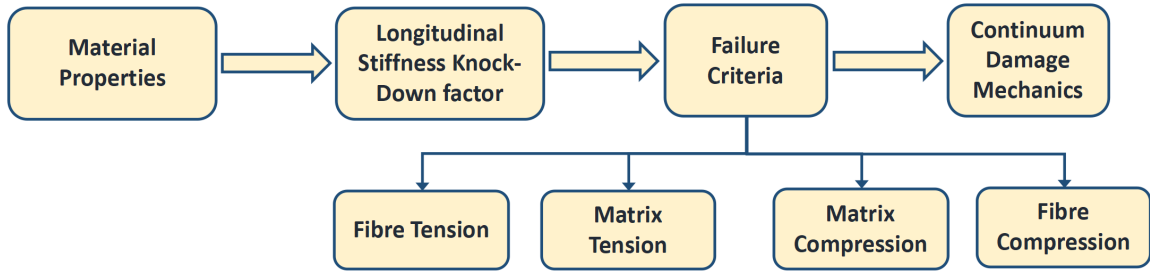


Figure 5.1: *Constitutive model planning overview.*

## 5.2 Material Properties - Mesomechanical Approach

The material properties calculations at layer level were based on the rule of mixtures and the methodology proposed by Olsson, Marklund and Asp. [104,105,185], which is developed on a conventional UD NCF model, assuming straight tows, at this stage, not influenced by the computed knock-down factors. This method is derived considering the iso-strain assumption. The stitches are included at a  $45^\circ$  ( $\omega$ ) orientation to the fibre direction (Fig. 5.2) due to the architecture of the current NCF, whereas in the previous studies stitching yarns were assumed parallel and perpendicular to the fibres. The longitudinal modulus  $E_{11}$  is obtained by a modified ROM (Eq. 5.1). The reason for selecting to estimate the mechanical properties at layer level instead of homogenising the entire NCF blanket is that the objective of this study is to explicitly predict the failure of each layer within the NCF blanket.

$$E_{11} = V_{Pes} \cdot E_{Pes_{45^\circ}}^{11} + V_M \cdot E_M + V_F \cdot E_{F11} \quad (5.1)$$

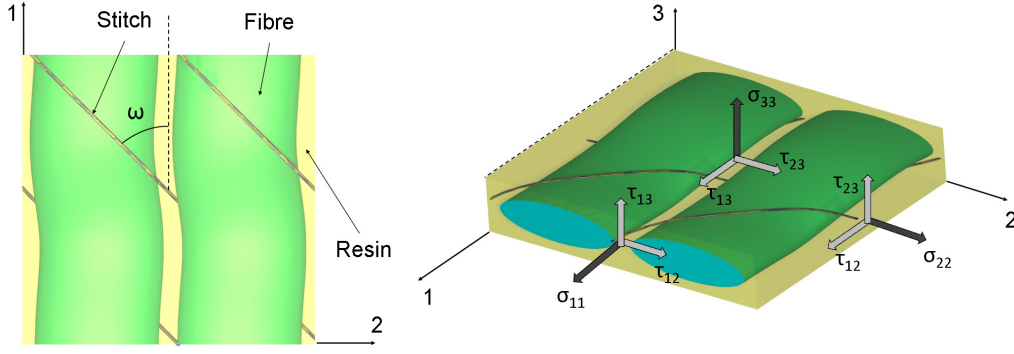


Figure 5.2: *Non-Crimp fabric illustration with pillar stitching pattern.*

where,  $V_{Pes}$ ,  $M$ ,  $F$  are the volume fractions of polyester, matrix and fibre respectively, with the values obtained from Chapter 3,  $E_{Pes45^\circ}^{11} = E_{Pes} \cdot \cos^3 \omega$  is the effective Young's modulus of polyester in longitudinal direction-1, which is obtained from the calculations presented in Appendix B.1.3, and  $E_M$ ,  $F_{11}$  are the modulus of the resin and the longitudinal modulus of the fibre respectively. An enhanced form of Halpin-Tsai relations [186], which includes the angular component of the stitches is implemented to calculate the transverse modulus,  $E_{22}$  (Eq. 5.2):

$$E_{22} = V_{Pes} \cdot E_{Pes45^\circ}^{22} + (1 - V_{Pes}) \cdot E_M \frac{(1 + \xi_E \cdot \eta_E \cdot V_F)}{(1 - \eta_E \cdot V_F)} \quad (5.2)$$

where,  $\eta_E = \frac{(\frac{E_{b22}}{E_M} - 1)}{(\frac{E_{b22}}{E_M} + \xi_E)}$ , with  $\xi_E = 2$  as advised by Olsson et al. [185].  $E_{Pes45^\circ}^{22} = E_{Pes} \cdot \sin^3 \omega$  is the effective Young's modulus of polyester in transverse direction-1, as obtained from the procedure shown in Appendix B.1.3. The magnitude  $E_{b22}$  is the transverse modulus of the fibre bundle given by Eq. 5.3:

$$E_{b22} = \frac{E_{F22} \cdot \left( \frac{E_M}{1 - \nu_M^2} \right)}{V_{Fb} \cdot \left( \frac{E_M}{1 - \nu_M^2} \right) + (1 - V_{Fb}) \cdot E_{F22}} \quad (5.3)$$

where,  $V_{Fb}$  is the fibre volume fraction within a bundle and  $\nu_m$  is the Poisson's ratio of matrix. For the linear shear modulus, the transverse polyester yarn is accounted to carry the transverse load, supplementary to the matrix. More specifically, as shown in Fig. 5.3, for the specific stitching pattern of the present fabric, it can be observed that if the layer is cut with the fibres

oriented in  $+45^\circ$  (Fig. 5.3a)), the stitches are aligned to the tensile load direction and thus enhance the shear modulus and shear strength at layer level, whereas in the event a  $-45^\circ$  fibre orientation layer (Fig. 5.3b)) is formed, the stitches are aligned perpendicular to the tensile load and do not contribute to the increase of either of these two magnitudes of the layer.

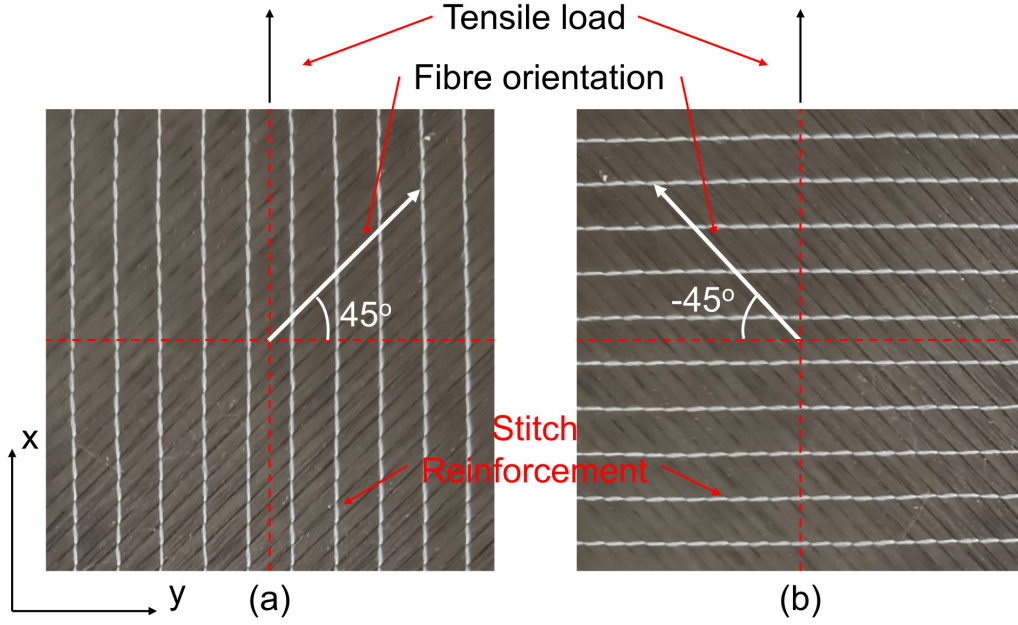


Figure 5.3: Illustration of: a) a  $+45^\circ$  and b)  $-45^\circ$  fibre orientation layer, denoting the orientation of the stitch reinforcement with respect to the fibre and tensile load direction.

A proposal to introduce this parameter to shear modulus and strength calculation is shown below:

$$G_{12Init.}^{+45^\circ} = \left( G_M \cdot \frac{V_M}{V_F} + E_{Pes} \cdot \frac{V_{Pes}}{V_F} \right) \frac{(1 + \xi_G \cdot \eta_G \cdot V_F)}{(1 - \eta_G \cdot V_F)} \quad (5.4)$$

$$G_{12Init.}^{-45^\circ} = \left( G_M \cdot \frac{V_M}{V_F} \right) \frac{(1 + \xi_G \cdot \eta_G \cdot V_F)}{(1 - \eta_G \cdot V_F)} \quad (5.5)$$

where,  $\eta_G = \frac{\left( \frac{G_{b12}}{G_M} - 1 \right)}{\left( \frac{G_{b12}}{G_M} + \xi_G \right)}$ , with  $\xi_G = 26$  as advised by Olsson et al. [185]. The parameter  $\xi_G$  is the reinforcing factor and depends on fibre geometry, packing geometry and loading condition.

The magnitude  $G_{b12}$  is the shear modulus of the fibre bundle and equals to Eq. 5.6:

$$G_{b12} = \left( \frac{V_{F_b}}{G_{F12}} + \frac{1 - V_{F_b}}{G_M} \right)^{-1} \quad (5.6)$$

The effect of waviness in fibre bundles is not accounted at this stage in Eq. 5.1, because it is later introduced in Section 5.3 of this document in the form of a knock-down factor. Moreover, matrix and shear stiffness (Eqs. 5.2, 5.4 and 5.5) have not been reported in previous studies to be influenced by fibre waviness, therefore deliberately fibre misalignment is not accounted. The Poisson's ratio of the layer is calculated by Eq. 5.7:

$$\nu_{12} = V_M \cdot \nu_M + V_{Pes} \cdot \nu_{Pes} + V_F \cdot \nu_{F12} \quad (5.7)$$

The strength prediction of an NCF layer in the fibre direction is assumed to be controlled by the fibre failure during loading along the bundles [185], thus the strength values in direction-1 for tension and compression are estimated as follows:

$$X_T = X_{T_b} \cdot \left( V_{F_b} + V_M \cdot \frac{E_M}{E_{b11}} + V_{Pes} \cdot \frac{E_{Pes} \cdot \cos 45}{E_{b11}} \right) \quad (5.8)$$

where,  $X_{T_b}$  is the tensile strength of the fibre bundle in (MPa) as obtained by Eq. 5.9:

$$X_{T_b} = X_{T_F} \cdot (V_{F_b} + (1 - V_{F_b}) \cdot E_M / E_{F11}) \quad (5.9)$$

with  $E_{b11}$  to be the Young's modulus of the fibre bundle in (MPa) as obtained by  $E_{b11} = V_{F_b} \cdot E_{F11} + (1 - V_{F_b}) \cdot E_M$ .

$$X_C = X_{C_b} \cdot \left( V_{F_b} + V_M \cdot \frac{E_M}{E_{b11}} + V_{Pes} \cdot \frac{E_{Pes} \cdot \cos 45}{E_{b11}} \right) \quad (5.10)$$

where,  $X_{C_b}$  is the compressive strength of the fibre bundle in (MPa) as obtained by Eq. 5.11.

$$X_{C_b} = X_{C_F} \cdot (V_{F_b} + (1 - V_{F_b}) \cdot E_M/E_{F_{11}}) \quad (5.11)$$

where,  $X_{C_F}$  is the compressive strength of carbon fibres in (MPa), calculated with a factor of 0.6 of the respective tensile strength based on the empirical correction according to the ratio of compressive and tensile strength in the WWFE II [111]. For Eqs. 5.1 - 5.11, the mechanical properties of the constituents, namely fibre and resin systems, are summarised in Appendix B.1.1 in Tabs. B.1, B.2 and B.3, whereas for the polyester stitches in Tab. 4.2 of Chapter 4. Furthermore, the mechanical properties at bundle level as a function of  $V_{F_b}$  are presented in Tab. B.4 of Appendix B.1.2. With regards to the transverse strengths (direction-2) which are dominated by the matrix strength and the enhancement of the tensile properties of the polyester stitches, Marklund et al. [104] proposed an analytical method to calculate them. However this method does not account the in-situ strengths, namely the interaction of one layer with neighbour layers and assumes that the transverse strength of each layer is the same for all layers through-the-thickness. This approach may not affect the prediction of the overall strength of a UD laminate, but that is certainly not the case for a cross-ply NCF composite, where the neighbouring layers of different orientations do enhance the resultant transverse tensile strength.

As a result of this, the author decided to use the in-situ transverse and shear strength formulation. In principle, the in-situ strengths were originally examined by Parvizi et al. [187] whose study revealed the enhanced - compared to the same ply of a UD laminate - transverse tensile and shear strengths of a ply or layer when it is constrained by layers with different fibre orientations in a laminate. Later, Camanho et al. [188] and Dvorak et al. [124] presented analytical formulations of in-situ strengths for UD layers. Although, they were originally developed for UD composites, the fundamental parameters that define the in-situ strengths are identical for NCF too, namely the thickness of the ply or layer, the lay-up of the laminate, the fracture toughness and their location within the laminate (Fig. 5.4 - outer or embedded layer). Furthermore, in recent studies for NCF [131], the in-situ strength formula for UD composites was successfully applied, therefore in the present work this approach is followed too.

The analytical formulations in transverse tension,  $Y_{TIS}$ , compression,  $Y_{CIS}$ , longitudinal,  $S_{LIS}$  and transverse shear,  $S_{TIS}$  can be found in [124,188,189] together with thorough analysis and validation. However, a brief overview of the theory is presented below.

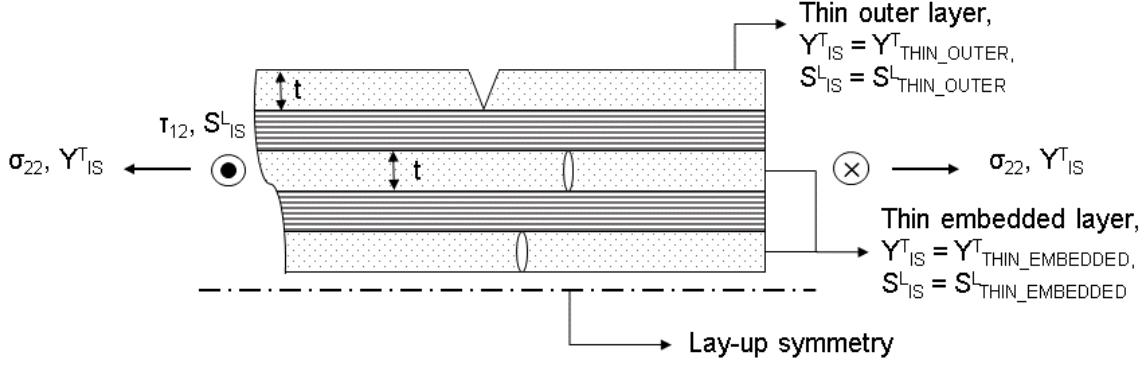


Figure 5.4: *Thin outer and embedded layer.*

The in-situ transverse tensile strength for a thin outer and thin embedded layer is expressed by Eqs. 5.12a) and b) [124,188]. Eqs. 5.12a) and b) reveal that the in-situ transverse tensile strength is dependant on Mode I intralaminar fracture toughness and the effective longitudinal and transverse moduli. The fracture toughness can be obtained from Mode I - DCB tests, although the specific contribution of the stitches is not explicitly quantified and requires further investigation.

$$Y_{IS}^T = \sqrt{\frac{4 \cdot G_{IC}}{\pi \cdot t \cdot \Lambda}} : \text{Thin outer layer} \quad (5.12a)$$

$$Y_{IS}^T = \sqrt{\frac{8 \cdot G_{IC}}{\pi \cdot t \cdot \Lambda}} : \text{Thin embedded layer} \quad (5.12b)$$

where,  $\Lambda = 2 \cdot (1/E_{22} - \nu_{21}^2/E_{11_{eff}})$  (1/MPa),  $G_{IC}$  is the transverse fracture toughness in Mode I (N/mm) and  $t$  is the thickness of the layer (mm).

Similarly, the in-situ longitudinal shear strength calculation (Eq. 5.13), the significant factors are the in-plane shear modulus and Mode II transverse fracture toughness. Parameter  $\beta$ , attributes the shear stress versus strain non-linear response and normally receives values between



2.0 - 3.6 x 10<sup>-8</sup> MPa<sup>-3</sup>. More detailed information about in-situ strength predictions can be found in [124, 188].

$$S_{IS}^L = \sqrt{\frac{(1 + \beta \cdot \phi \cdot (G_{12_{Init.}}^j)^2)^{1/2} - 1}{3 \cdot \beta \cdot G_{12}}} \quad (5.13)$$

$$\phi = \frac{24 \cdot G_{IIC}}{\pi \cdot t} : \text{Thin outer layer} \quad (5.14a)$$

$$\phi = \frac{48 \cdot G_{IIC}}{\pi \cdot t} : \text{Thin embedded layer} \quad (5.14b)$$

where,  $G_{12_{Init.}}^j$  is the in-plane shear modulus (GPa),  $j$  denotes the -45° or 45° layer and  $G_{IIC}$  is the transverse fracture toughness in Mode II (N/mm). Catalanotti et al. [189, 190] proposed that the in-situ strength of transverse compression,  $Y_{IS}^C$  and shear,  $S_{IS}^T$  can be estimated as a function of  $S_{IS}^L$ , the longitudinal friction coefficient,  $\eta_L$  and the fracture plane angle under pure compression,  $\alpha_0$ . The analytical formulations are presented in Eqs. 5.15 and 5.16 respectively.

$$Y_{IS}^C = \frac{(1 - 2 \cdot \cos^2 \alpha_0)}{\eta_L \cdot \cos^2 \alpha_0} \cdot S_{IS}^L \quad (5.15)$$

$$S_{IS}^T = \frac{(2 \cdot \sin^2 \alpha_0 - 1)}{\eta_L \cdot \sin 2\alpha_0} \cdot S_{IS}^L \quad (5.16)$$

### 5.3 Effective Longitudinal Modulus

As reported earlier in the thesis, the internal structure of NCF composites is characterised by a certain amount of in- and out-of-plane fibre waviness, which ultimately reduces the tensile, but more significantly, the compressive longitudinal modulus of the composite. The tow crimp reduces the stiffness of the 0° layer and causes incorrect predictions of the laminate elastic properties employing assumption of straight fibres. In this project it has been opted to represent

the waviness of the  $0^\circ$  layer as flat layer with an effective stiffness in function of the in-plane and out-of-plane fibre misalignment angles. The transverse stiffness of the  $90^\circ$  layer, according to author's knowledge, is not reported in the literature to be degraded due to tow waviness, on the contrary, stitches provide an additional source of delaying the resin crack openings. Mattsson et al. [36] in their study on NCF cross-ply reported that generally the stiffness of the  $90^\circ$  layers in the transverse direction can be neglected when compared to the overall stiffness of the laminate. Hence, it has been decided not to introduce a stiffness degradation model for transverse tensile or compressive loads.

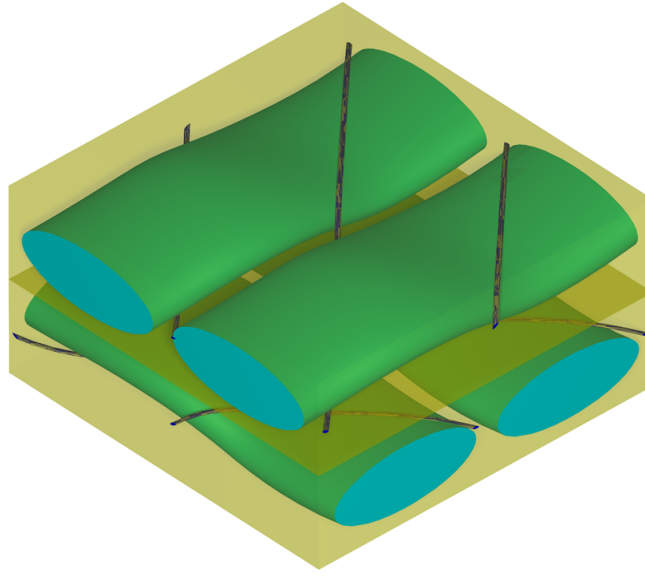


Figure 5.5: *Illustration of a biaxial representative NCF composite with pillar stitching pattern.*

A commonly used strategy, as suggested by Cox and Dadkhah [108], is to calculate the knock-down factor,  $k$  as a sinus function of the wavelength,  $\lambda$  and amplitude,  $A$  of the curved bundle (Eq. 5.17). However, this approach assumes a periodic fibre waviness which is not always the case as explained in Chapter 3 and this waviness is accounted only at one plane.

Therefore in this work, a new KD, ( $k$ ) model is proposed, which is employed at layer level, composed by two individual KD subfactors due to the in-plane ( $k_{IP}$ ) and out-of-plane ( $k_{OP}$ ) fibre misalignment. The initial misalignment angles, -  $\phi_I$  and  $\theta_I$  - are introduced instead of the wave properties as more representative to the actual NCF architecture and they are assumed to equally contribute to the Young's modulus knock-down factor, although  $\phi_I$  is significantly smaller compared to  $\theta_I$ , as presented in Tab. 3.3. The ratio  $A/\lambda$  in Eq. 5.17 can be replaced

by  $\frac{\tan \theta_i}{2\pi}$  or  $\phi_i$  as illustrated in Fig. 5.6.

$$k = \frac{1}{1 + 2 \cdot \left(\frac{\pi A}{\lambda}\right)^2 \cdot \left[\frac{E_{11}}{G_{12}} - 2 \cdot (1 + \nu_{12})\right]} \quad (5.17)$$

Hence, Eqs. 5.18 can be developed. As assumption, it is obtained that all fibre tows are wavy to the same degree, whereas in reality, according to author's observations, there is a variance in the degree of waviness from laminate to laminate, specimen to specimen and from tow to tow even within the same specimen. The effective longitudinal modulus,  $E_{11_{eff}}$ , of a fibre bundle within a NCF layer is now obtained in the form of:  $E_{11_{eff}}/E_{11}$ , where  $E_{11}$  corresponds to the stiffness of the layer calculated with the rule of mixtures (Section 5.2).

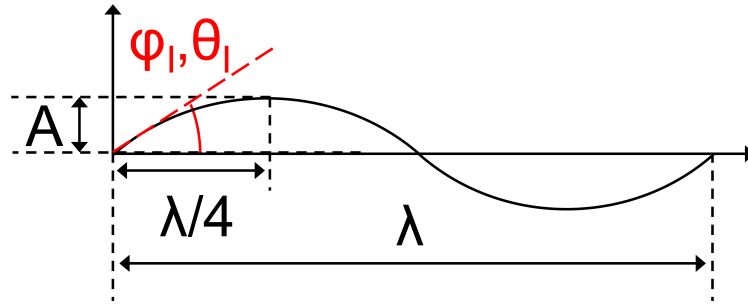


Figure 5.6: Schematic showing fibre undulation expressed as angular misalignment (Not scaled, for reference only).

$$k = k_{IP} \cdot k_{OP}, \quad \begin{cases} k_{IP} = \frac{1}{1 + \frac{\tan^2 \phi_I}{2} \cdot \left[\frac{E_{11}}{G_{12}^{i_{Init.}}} - 2 \cdot (1 + \nu_{12})\right]} \\ k_{OP} = \frac{1}{1 + \frac{\tan^2 \theta_I}{2} \cdot \left[\frac{E_{11}}{G_{13}} - 2 \cdot (1 + \nu_{13})\right]} \end{cases} \quad (5.18)$$

where,  $G_{12_{Init.}}$  is the linear in-plane shear modulus,  $G_{13}$ ,  $\nu_{13}$  are out-of-plane shear moduli and Poisson's ratio of the  $0^\circ$  layer. The denominator of Eqs. 5.18 equals to 1 for straight tows, while the magnitude within the brackets corresponds to the anisotropy factor. The fractional loss of modulus,  $1 - k$ , rises as  $\theta_I$  and  $\phi_I$  increase, also Eq. 5.18 holds if the waviness takes the form of a continuously varying misalignment angle (Fig. 5.7). Fig. 5.7 depicts the evolution of  $k$  as  $\theta_I$  and  $\phi_I$  increase. Thus, the effective Young's modulus in the longitudinal direction

equals:  $E_{11_{\text{Eff}}} = k \cdot E_{11}$ .

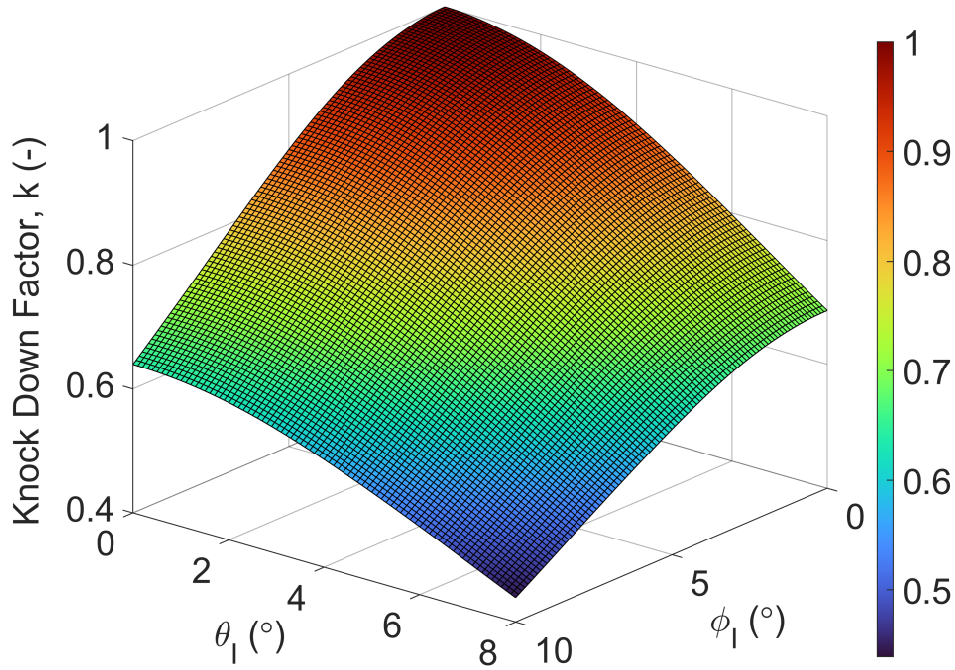


Figure 5.7: Longitudinal stiffness KD factor development for different values of  $\phi_I$  and  $\theta_I$ .

## 5.4 Failure Criteria for NCF Composites

Several failure criteria tailored for NCF have already been published [22–24, 26, 53, 98, 104, 112, 113, 129, 130] and they have been discussed in literature review (Chapter 2) of this thesis. However, some areas for further improvement have been identified; namely, the introduction of the mechanical properties of the stitches into the tensile properties of NCF composites, which until now are neglected, as well as the further development of compression failure criterion which incorporates both the in- and out-of-plane fibre waviness. Hence, in this work a set of physically-based failure criteria is demonstrated and further development is made to the failure criteria that were originally introduced to the energy-based failure model for UDPT [191] and woven [13] composites.

### 5.4.1 Tensile fibre mode, $\sigma_{11} > 0$

The tensile strength can be predicted fairly well from the stress estimated in individual tows in an isostrain model. Fibre failure is interpreted as a fracture of a large amount of elementary filaments causing a loss of the fibre parallel to load bearing capacity within a macroscopic area of the  $0^\circ$  layer. The maximum stress criterion (Eq. 5.19) is considered suitable to quantify the failure index in fibre tension, however the fibre tensile strength,  $X_T$  incorporates the effect of the stitching binder as it was explained in Section 5.2.

$$FI_{FT} = \frac{\sigma_{11}}{X_T} = 1 \quad (5.19)$$

### 5.4.2 Compressive fibre mode, $\sigma_{11} < 0$

The fibre compressive failure in NCF composites follows, in principle, the same physics as other types of composites. As explained in Section 4.5, kink band formations are observed in compressive failure, which could either be the final result of micro-buckling of fibres, or, as assumed by Argon [47], to be triggered by the localised matrix failure in the areas where misaligned fibres exist. The conclusion from these hypotheses is the initiation of kinking can be attributed to local microstructural defects such as fibre misalignments and longitudinal cracking, therefore, the phenomenon is better comprehended when it is treated with stress redistribution, including dynamics, geometric and material non-linearities as suggested by Pinho et al. [127, 192].

In this study, the microstructural defects are expressed in the form of fibre waviness; however, unlike previous studies, both the in- and out-of-plane fibre waviness are accounted to form the failure criterion. Although the out-of-plane waviness is believed to be the most critical form of imperfection within the NCF composite in kink band formation under compressive loads, in-plane waviness plays an important role too. Plastic shear instability occurs where initially misaligned fibres cause shear strains to localise in a small region [113] in- and out-of-plane. Fibre fails into small segments, which rotate under increased load. The maximum stress criterion which is implemented in the original version of the energy-based model [191], does not

include any of these parameters as an independent failure option. In this work, the proposed failure model assumes initial in- and out-of-plane fibre misalignment and it is based on Argon's hypothesis [47] and on the subsequent developments of Davila et al. [123] and Pinho et al. [127].

Considering a unidirectional NCF layer under a general compressive stress state as indicated by matrix 5.20, the fibre kinking plane is assumed to be at an angle  $\psi$  with axis 2, i.e. inclined to the transverse direction of a bundle. The value of  $\psi$  angle ranges between 0 and  $180^\circ$  (Fig. 5.8) and is found iteratively; so as to maximise the failure index, which is later presented in Eq. 3.38. The coordinate system associated with the kink plane is  $1^{(\psi)}2^{(\psi)}3^{(\psi)}$  and it is obtained by the rotation of frame 123 around axis 1 of that angle  $\psi$  (Fig. 5.8). Fig. 5.8 shows the different coordinate systems and the stresses acting on the kink plane. The coordinate system 123 is aligned with the material axes of the composite: 1 represents the fibre direction, 2 and 3 the transverse directions.

$$\sigma = \begin{bmatrix} \sigma_{11} & \sigma_{12} & \sigma_{13} \\ \sigma_{21} & \sigma_{22} & \sigma_{23} \\ \sigma_{31} & \sigma_{32} & \sigma_{33} \end{bmatrix} \quad (5.20)$$

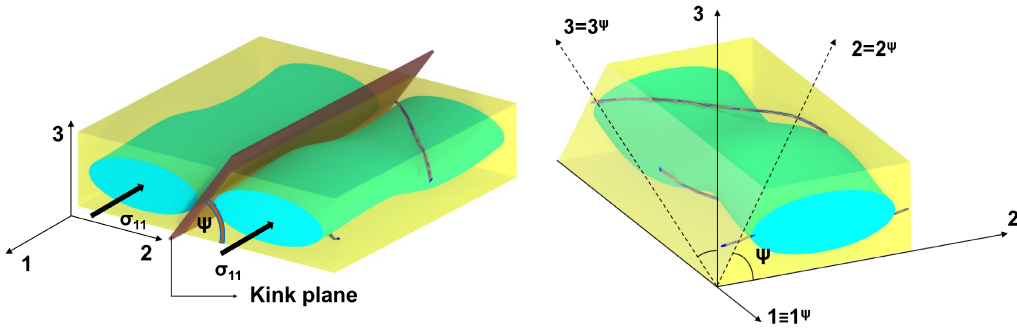


Figure 5.8: *Cross-ply NCF volume element (left), fracture plane illustration (right).*

By applying the rotation  $\psi$ , the stress tensor in the new coordinate system results:

$$\sigma_\psi = \begin{bmatrix} R_\psi \end{bmatrix} \cdot \begin{bmatrix} \sigma \end{bmatrix} \cdot \begin{bmatrix} R_\psi^T \end{bmatrix} \quad (5.21)$$

where the rotation and transposed rotation matrices are defined as:

$$R_\psi = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \psi & \sin \psi \\ 0 & -\sin \psi & \cos \psi \end{bmatrix}, \quad R_\psi^T = \begin{bmatrix} 1 & 0 & 0 \\ 0 & \cos \psi & -\sin \psi \\ 0 & \sin \psi & \cos \psi \end{bmatrix} \quad (5.22)$$

Since the current energy-based damage mechanics model [12] is developed for in-plane loading, the following stress conditions are applied.

$$\sigma_{11}, \sigma_{12}, \sigma_{21}, \sigma_{22} \neq 0, \quad \sigma_{33} = \sigma_{13} = \sigma_{31} = \sigma_{23} = \sigma_{32} = 0 \quad (5.23)$$

The stress components of the new coordinate system are expressed as:

$$\begin{aligned} \sigma_{11}^\psi &= \sigma_{11}, & \sigma_{12}^\psi &= \sigma_{21}^\psi = \sigma_{12} \cdot \cos \psi, & \sigma_{13}^\psi &= \sigma_{31}^\psi = -\sigma_{12} \cdot \sin \psi \\ \sigma_{22}^\psi &= \sigma_{22} \cdot \cos^2 \psi \\ \sigma_{23}^\psi &= \sigma_{32}^\psi = -\frac{\sigma_{22}}{2} \cdot \sin(2\psi), & \sigma_{33}^\psi &= -\sigma_{22} \cdot \sin^2 \psi \end{aligned} \quad (5.24)$$

The total misalignment angle can be decomposed into an initial (constant) misalignment angle  $\phi_I$  or  $\theta_I$  that represents a manufacturing imperfection, and they are experimentally measured as explained in Chapter 3, and an additional rotational component  $\phi_R$  or  $\theta_R$  that results from shear loading. Equations 5.25 and 5.26 describe the total misalignment angles for in- and out-of-plane fibre waviness which can also be identified in Figs. 5.9 and 5.10.

$$\phi = \phi_I + \phi_R \quad (5.25)$$

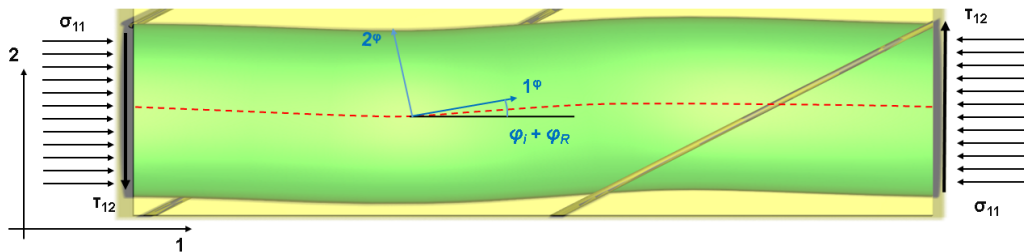


Figure 5.9: Illustration of in-plane fibre tow misalignment.

$$\theta = \theta_I + \theta_R \quad (5.26)$$

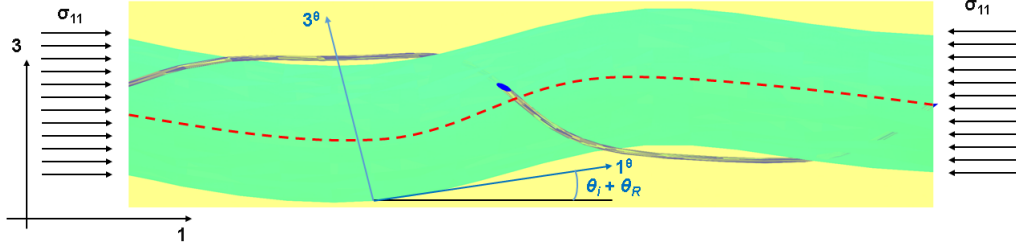


Figure 5.10: *Illustration of out-of-plane fibre tow misalignment.*

As suggested by Davila et al. [123], the additional rotation components are calculated using small angle approximations from Eqs. 5.27a) and b). It has to be noted that since each misalignment occurs in different planes, namely plane 1-2 and 1-3, the corresponding material properties need to be used.

$$\phi_R = \frac{-\phi_I \cdot \sigma_{11} + \phi_I \cdot \sigma_{22} + |\sigma_{12}|}{G_{12}} \quad a), \quad \theta_R = \frac{-\theta_I \cdot \sigma_{11} + \theta_I \cdot \sigma_{33} + |\sigma_{13}|}{G_{13}} \quad b) \quad (5.27)$$

Furthermore, since only in-plane loading cases are accounted,  $\sigma_{33}$  and  $\sigma_{13}$  from Eq. 5.27 b) are equal to zero, thus 5.27 b) receives the form:

$$\theta_R = \frac{-\theta_I \cdot \sigma_{11}}{G_{13}} \quad (5.28)$$

The in- and out-of-plane fibre misalignments and the respective coordinate systems  $(1^{(\phi)}2^{(\phi)}3^{(\phi)})$  and  $(1^{(\theta)}2^{(\theta)}3^{(\theta)})$  are rotated at an angle  $\phi_I$  and  $\theta_I$  respectively around the axis  $3^{(\psi)}$ :

$$R_{\phi_I} = \begin{bmatrix} \cos \phi_I & \sin \phi_I & 0 \\ -\sin \phi_I & \cos \phi_I & 0 \\ 0 & 0 & 1 \end{bmatrix}, \quad R_{\theta_I} = \begin{bmatrix} \cos \theta_I & 0 & \sin \theta_I \\ 0 & 1 & 0 \\ -\sin \theta_I & 0 & \cos \theta_I \end{bmatrix} \quad (5.29)$$

After these two rotations, the resultant rotation  $R_m$ , in the misalignment coordinate system



$1^m 2^m 3^m$  can be defined as:

$$R_m = \begin{bmatrix} R_{\phi_I} \end{bmatrix} \cdot \begin{bmatrix} R_{\theta_I} \end{bmatrix} = \begin{bmatrix} \cos \theta_I \cdot \cos \phi_I & \cos \theta_I \cdot \sin \phi_I & \sin \theta_I \\ -\sin \phi_I & \cos \phi_I & 0 \\ -\sin \theta_I \cdot \cos \phi_I & -\sin \theta_I \cdot \sin \phi_I & \cos \theta_I \end{bmatrix} \quad (5.30)$$

The stress tensor of the misaligned coordinate system is then obtained from Eq. 5.31.

$$\sigma_m = \begin{bmatrix} R_m \end{bmatrix} \cdot \begin{bmatrix} \sigma_\psi \end{bmatrix} \cdot \begin{bmatrix} R_m^T \end{bmatrix} \quad (5.31)$$

Having defined the stress in the misalignment frame the components of the tractions are:

$$\left\{ \begin{array}{l} \sigma_{11}^m = \cos^2 \theta_I \cdot \left( \sigma_{11}^\psi \cdot \cos^2 \phi_I + \sigma_{21}^\psi \cdot \sin 2\phi_I + \sigma_{22}^\psi \cdot \sin^2 \phi_I \right) \\ \quad + \cos 2\theta_I \cdot \left( \sigma_{31}^\psi \cdot \cos \phi_I + \sigma_{23}^\psi \cdot \sin \phi_I \right) + \sigma_{33}^\psi \cdot \sin^2 \theta_I \\ \sigma_{12}^m = \sigma_{21}^m = \cos \theta_I \cdot \left( \left( \sigma_{22}^\psi - \sigma_{11}^\psi \right) \cdot \frac{\sin 2\theta_I}{2} + \sigma_{21}^\psi \cdot \cos 2\phi_I \right) - \sin \theta_I \cdot \left( \sigma_{13}^\psi \cdot \sin \phi_I - \sigma_{23}^\psi \cdot \cos \phi_I \right) \\ \sigma_{13}^m = \sigma_{31}^m = -\frac{\sin 2\theta_I}{2} \cdot \left( \sigma_{11}^\psi \cdot \cos^2 \phi_I + \sigma_{21}^\psi \cdot \sin 2\phi_I + \sigma_{22}^\psi \cdot \sin^2 \phi_I - \sigma_{33}^\psi \right) \\ \quad + \cos 2\theta_I \cdot \left( \sigma_{31}^\psi \cdot \cos \phi_I + \sigma_{23}^\psi \cdot \sin \phi_I \right) \\ \sigma_{22}^m = \sigma_{11}^\psi \cdot \sin^2 \phi_I + \sigma_{22}^\psi \cdot \cos^2 \phi_I - \sigma_{21}^\psi \cdot \sin 2\phi_I \\ \sigma_{23}^m = \sigma_{32}^m = \sin \theta_I \cdot \left( \left( \sigma_{11}^\psi - \sigma_{22}^\psi \right) \cdot \frac{\sin 2\theta_I}{2} - \sigma_{21}^\psi \cdot \cos 2\phi_I \right) + \cos \theta_I \cdot \left( \sigma_{23}^\psi \cdot \cos \phi_I - \sigma_{13}^\psi \cdot \sin \phi_I \right) \\ \sigma_{33}^m = \sin^2 \theta_I \cdot \left( \sigma_{11}^\psi \cdot \cos^2 \phi_I + \sigma_{21}^\psi \cdot \sin 2\phi_I - \sigma_{22}^\psi \cdot \sin^2 \phi_I \right) \\ \quad - \sin 2\theta_I \cdot \left( \sigma_{31}^\psi \cdot \cos \phi_I - \sigma_{23}^\psi \cdot \sin \phi_I \right) + \sigma_{33}^\psi \cdot \cos^2 \theta_I \end{array} \right.$$

The proposed failure criterion is based on the form of LaRC05 [127] failure model, however, two additional stress terms (C and E) due to the extra fibre misalignment are introduced. Term C of Eq. 5.32 corresponds to the shear-dominated matrix failure in the misaligned frame due to the fibre misalignment in plane 1-3, under significant longitudinal compression. Similarly, term E is attributed to the condition of longitudinal compression combined with transverse tension.

If angle  $\theta$  is assumed to be zero, namely induced fibre misalignment in plane 1-2 only, then  $\sigma_{13}^m$  and  $\sigma_{33}^m$  will become zero. Hence, Eq. 5.32 is reformed to the failure mode for fibre kinking of LaRC05.

$$FI_{FK} = \overbrace{\left(\frac{\sigma_{23}^m}{S_{TIS} - \eta_T \cdot \sigma_{22}^m}\right)^2}^A + \overbrace{\left(\frac{\sigma_{12}^m}{S_{LIS} - \eta_L \cdot \sigma_{22}^m}\right)^2}^B + \overbrace{\left(\frac{\sigma_{13}^m}{S_{LIS} - \eta_L \cdot \sigma_{33}^m}\right)^2}^C + \overbrace{\left(\frac{\langle \sigma_{22}^m \rangle_+}{Y_{TIS}}\right)^2}^D + \overbrace{\left(\frac{\langle \sigma_{33}^m \rangle_+}{Z_T}\right)^2}^E = 1 \quad (5.32)$$

where,  $\eta_T$  and  $\eta_L$  are the internal material frictions,  $\eta^T = -\frac{1}{\tan 2a_0}$  and  $\eta^L$ ,  $Y_T^{IS}$  is the in-situ transverse tensile strength (direction 2) (MPa),  $Z_T$  is the transverse tensile strength (direction 3) (MPa),  $S_L^{IS}$  is the in-situ longitudinal shear strength (MPa) and  $S_T^{IS}$  is the in-situ transverse shear strength of the bundle material (MPa). The transverse compression  $\sigma_{22}^m$  and  $\sigma_{33}^m$  increase the shear strength as explained in Puck's model [119] since friction is induced to the fracture plane.

### 5.4.3 Tensile matrix mode, $\sigma_{22} > 0$

In the original version of the energy-based constitutive model, the damage initiation due to tensile transverse loads is described by a quadratic interaction criterion of shear stress  $\tau_{12}$  and transverse direct stress  $\sigma_{22}$ :

$$\left(\frac{\tau_{12}}{S_C}\right)^2 + \left(\frac{\sigma_{22}}{Y_T}\right)^2 \leq 1 \quad (5.33)$$

However, the quadratic law of Eq. 5.33 only accounts the shear and matrix tensile strengths of a UD layer. Since in the present study the examined NCF composite configuration is formed by biaxial NCF, it was considered essential to implement to the failure criterion the in-situ strengths for transverse tension and longitudinal shear, which results that Eq. 5.33 is updated to the LaRC03 [123] failure criterion (Eq. 5.34) for transverse tensile loading. The normal

stress term of Eq. 5.34 represents the contribution from the positive normal traction in opening the cracks, whereas the shear term corresponds to matrix failure initiation within bundles or adhesive failure of the fibre/matrix interface which, due to inherent bundle waviness of NCF composites, may exhibit under in-plane loading.

$$FI_{MT} = \left( \frac{\tau_{12}}{S_{LIS}} \right)^2 + \left( \frac{\sigma_{22}}{Y_{TIS}} \right)^2 \leq 1 \quad (5.34)$$

#### 5.4.4 Compressive matrix mode, $\sigma_{22} < 0$

From the experimental study performed in this project, the transverse compression behaviour of biaxial NCF composites does not appear and neither has been reported by other studies, to be any different from the UDPT composites. The interaction of transverse direct stresses and shear stresses can have a significant effect. Transverse compressive stresses increase the material original shear strength while tensile stresses in transverse direction can reduce the shear stresses at damage initiation.

The strengthening effect of transverse compressive stresses is expressed through the Mohr-Coulomb criterion. In this case damage initiation is calculated on the base of the effective shear stresses  $\tau_{eff}^T$  and  $\tau_{eff}^L$  acting in the fracture plane  $a$  (Fig. 5.11). Namely, the prediction of the critical combination of shear and normal stresses acting on a potential inclined through-the-thickness fracture plane causing fracture can be achieved with the Mohr-Coulomb failure criterion (Eq. 5.11).

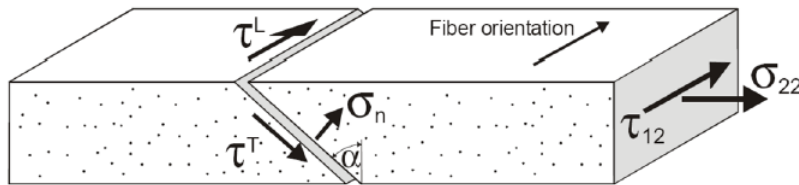


Figure 5.11: *Fracture of UD ply under transverse compression and shear loading [2].*

$$I(a) = \left( \frac{\tau_{eff}^T}{S_C^T} \right)^2 + \left( \frac{\tau_{eff}^L}{S_C^L} \right)^2 \leq 1 \quad (5.35)$$

where:

$$\tau_{eff}^T = \langle -\sigma_{22} \cos a (\sin a - \eta^T \cos a) \rangle \quad (5.36)$$

$$\tau_{eff}^L = \langle \cos a (|\tau_{12}| - \eta^L \sigma_{22} \cos a) \rangle \quad (5.37)$$

The fracture angle,  $\alpha$  varies between  $0^\circ$  and  $53^\circ$  with the extreme values referring to pure shear loading and pure compression respectively. Fig. 5.12 depicts the evolution of the failure envelope - i.e., failure index - as a combination of in-plane shear stress,  $\tau_{12}$  and transverse compression stress,  $\sigma_{22}$  for different values of fracture angles,  $\alpha$ . The enclosed shaded area corresponds to the worst case scenario, where the failure index reaches the maximum values for varying values of  $\alpha$ .

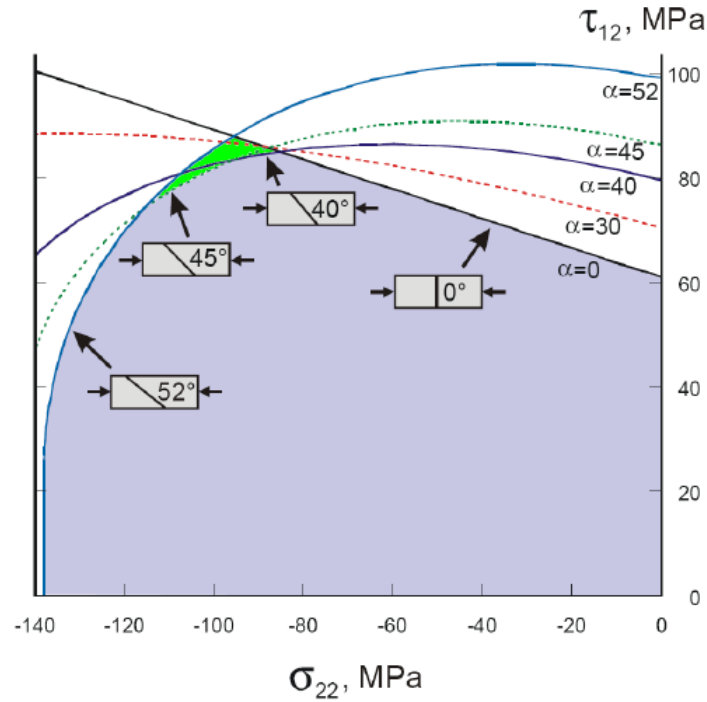


Figure 5.12: *Failure envelopes for different fracture angles [2].*

In the current project, as for the transverse tension case, it has been opted to implement the in-situ strengths to the failure criteria, hence, the failure criterion of the original model for matrix compression is modified to the following form:

$$FI_{MC} = \left( \frac{\tau_{eff}^T}{S_{IS}^T} \right)^2 + \left( \frac{\tau_{eff}^L}{S_{IS}^L} \right)^2 \leq 1 \quad (5.38)$$

The maximum value of the criterion is reached when:

$$FI_{MC}(a) = \text{Max} \{ FI_{MC}(a = 0^\circ), I(FI_{MC} = 53^\circ) \} \quad (5.39)$$

## 5.5 Continuum Damage Mechanics

The analytical formulations of sections 5.2 and 5.3, as well as the proposed set of failure criteria are implemented to an existing energy-based damage mechanics model developed by Iannucci et al. [12, 13, 184]. The model is based on the Continuum Damage Mechanics method and follows a layer-wise approach. It is capable to predict the mechanical behaviour of thin laminated and woven composites under quasi-static and impact loads and in the present work is extended to NCF composites. An important advantage of this approach is that with a single subroutine the mechanical response of structures made of different types of composites can be predicted, which is convenient for hybrid structures. Thanks to the layer-wise approach of the model, the failure of the NCF composite can be simulated at each layer of an NCF blanket for different failure modes, while the option to model a homogenised blanket is maintained. Also, because of the plane-stress formulation introduced into the damage model, continuum shell elements with reduced integration are applicable, which offers significant computational time savings when compared against solid element analyses. Because of the Classical Laminate Theory, the stresses through-the-thickness are considered to be zero and the transverse shear stresses are constant. Extensive details about the analytical formulation of the energy-based damage model can be found in [12, 13, 135], in the following subsections, though, an overview of the model is presented.

### 5.5.1 Effective stress-strain relationship

This damage mechanics approach introduces five damage variables for in-plane damage per NCF layer (one cross-ply NCF blanket is made of a  $0^\circ$  and a  $90^\circ$  layer) and is based on the principal of strain equivalence developed by Kachanov and Rabotnov [193,194] which is defined as:

$$\bar{\sigma} = \frac{\sigma}{1 - d} \quad (5.40)$$

The damage variables are associated with warp and weft fibre damage in both tensile and compressive failure modes, with an additional damage variable to determine the deterioration of the fibre matrix-interface. They are directly related to the stiffness degradation within the composite laminae and ultimately within the laminate. The evolution of damage in each mode is controlled via a series of damage-strain equations, thus allowing the total energy dissipated for each damage mode to be set as a material parameter. Damage modes are subdivided according to the stresses acting in respect to the local material orientation of the considered layer:

- $d_1$  = degradation of  $E_{11_{Eff.}}$  due to tensile stresses in longitudinal direction,
- $d_2$  = degradation of  $E_{22}$  due to tensile stresses in transverse direction,
- $d_3$  = degradation of  $G_{12_{Init.}}$  due to in-plane shear stresses,
- $d_4$  = degradation of  $E_{11_{Eff.}}$  due to compressive stresses in longitudinal direction,
- $d_5$  = degradation of  $E_{22}$  due to compressive stresses in transverse direction.

The degradation of  $E_{11_{Eff.}}$ ,  $E_{22}$  and  $G_{12_{Init.}}^j$  is defined as shown in Eqs. 5.41 and 5.42:

$$E_{ii}^d = (1 - d_l) \cdot E_{ii} \quad (5.41)$$

$$G_{12_{Init.}}^{d,j} = (1 - d_3) \cdot G_{12_{Init.}}^j \quad (5.42)$$

where  $i=1,2$  and  $l=1-2,4-5$  while  $d=0$  represents a virgin material and  $d=1$  the fully damaged

material. Also, the Poisson's ratios are degraded in a similar manner to stiffness, after satisfying the following relationship:  $\frac{\nu_{12}}{E_{11_{Eff}}} = \frac{\nu_{21}}{E_{22}}$ . Namely:

$$\nu_{12}^d = (1 - d_x) \cdot \nu_{12} \quad (5.43)$$

and the minor Poisson's ratio is given by:

$$\nu_{21}^d = (1 - d_y) \cdot \nu_{21} \quad (5.44)$$

where,  $(1 - d_x) = (1 - d_1)(1 - d_4)$  and  $(1 - d_y) = (1 - d_2)(1 - d_5)$

For the plane stress conditions, the orthotropic elastic matrix is defined as:

$$\epsilon = C \cdot \sigma \quad (5.45)$$

where  $C$  is the stiffness tensor which is given by:

$$\begin{bmatrix} 1/E_{11_{Eff}} & -\nu_{21}/E_{22} & 0 \\ -\nu_{12}/E_{11_{Eff}} & -1/E_{22} & 0 \\ 0 & 0 & 1/G_{12_{Init}}^j \end{bmatrix} \quad (5.46)$$

The stress-strain response with the damage variables includes, can be re-written as:

$$\begin{bmatrix} \sigma_{11} \\ \sigma_{22} \\ \tau_{12} \end{bmatrix} = \frac{1}{N} \cdot \begin{bmatrix} (1 - d_x)E_{11_{Eff}} & (1 - d_x)(1 - d_y)\nu_{21}E_{11_{Eff}} & 0 \\ (1 - d_y)(1 - d_x)\nu_{12}E_{22} & (1 - d_y)E_{22} & 0 \\ 0 & 0 & N(1 - d_3)/G_{12_{Init}}^j \end{bmatrix} \cdot \begin{bmatrix} \epsilon_{11} \\ \epsilon_{22} \\ \gamma_{12} \end{bmatrix} \quad (5.47)$$

where  $N$  is given by:  $N = 1 - (1 - d_x)(1 - d_y)\nu_{12}\nu_{21} > 0$

### 5.5.2 Damage evolution of direct stresses

Damage propagation is described in terms of the directly related true strains. The direct stress components, in transverse tension and compression in both directions, are expressed by a bi-linear stress-strain behaviour (Fig. 5.13). Hence, the stress in the damaged material at each current strain level  $\epsilon > \epsilon_{0,i}$  is defined in Fig. 5.13 as, OABC for tension and OABDE for compression. However, in compression, point B corresponds to a point with compressive failure, whereas BD relates to the residual strength ( $X_{CR}$ ) of the kink and matrix crush zone until failure occurs at a strain  $\epsilon_{max,R}$ . Unlike tension, the compressive stress converges on the residual strength. Additionally,  $E_C$  is defined as the energy dissipated due to 'plastic' deformation of the crushed zone. The bi-linear section of the damage evolution curve is defined as the following relationship:

$$d_i = \frac{\epsilon_{max,i}}{\epsilon_{max,i} - \epsilon_{0,i}} \cdot \left[ 1 - \frac{\dot{\alpha}_i \cdot \epsilon_{0,i}}{\epsilon_{ii}} \right] \quad (5.48)$$

where  $\dot{\alpha}_i$  is the strain-rate enhancement. Hence, stress in the damaged material at each current strain level  $\epsilon > \epsilon_{0,i}$  is calculated as:

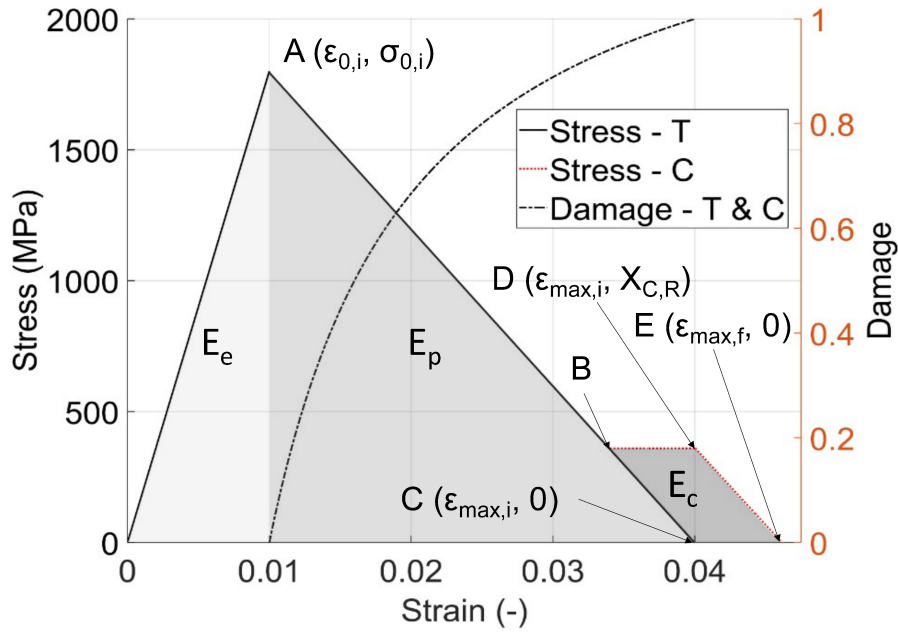
$$\sigma(\epsilon) = \frac{\sigma_C}{\epsilon_{max} - \epsilon_{0,i}} \cdot (\epsilon_{max} - \epsilon_{0,i}) \quad (5.49)$$

The general formulation to calculate the maximum strain, at ultimate damage, for fibre and matrix compression and matrix tension is:

$$\epsilon_{max,ii} = 2 \cdot \frac{G_{ICT/C}^\xi}{\sigma_{0,ii} \cdot l_x} \quad (5.50)$$

where  $G_{ICT/C}^\xi$  is the fracture toughness in tension (ICT) or compression (ICC), with  $\xi$  to be either  $0^\circ$  or  $90^\circ$  defining fibre or matrix direction respectively. Also,  $l_x$  is the characteristic length of a finite element, obtained from  $l_x = \sqrt{A_e}$ , with  $A_e$  being the in-plane area of the



Figure 5.13: *Bi-linear law for direct stresses.*

finite element. With regards to the maximum strain for fibre tension, that is obtained from a different formulation, due to the use of the exponential unloading law and is later presented in Section 5.6 of this thesis. The damage propagation path is re-calculated at every time step to account for load redistribution and changes in damage ratio.

### 5.5.3 In-plane shear component

The non-linear in-plane shear response of cross-ply NCF as described in Section 4.4 and is modelled in a 'piece-wise' incremental form with an exponential function. When the ultimate shear strength is reached a linear softening law follows until shear stress becomes zero. The in-plane shear response is shown in Fig. 5.14, which is described by an initial linear section ( $\gamma = 0$  to  $\gamma_0$ ), followed by a non-linear part. Then a nearly linear ultimate response at reduced modulus is observed until failure.

Hence, in terms of modelling, the linear region is described by  $G_{12_{Init}}^j$  and  $S_o$ , which is the stress where the response changes from linear to non-linear. The non-linear transition is defined by  $\lambda$ , the degree of non-linearity, and it is accounted as the damage initiation region which grows throughout the ultimate linear stage. Finally, the ultimate linear modulus is defined by  $G_{Ult}$ . The above-mentioned parameters are obtained from the in-plane shear tests. Hence the shear

modulus is defined by Eq. 5.51 as the greater of  $G_{Ult}$  and the non-linear transition modulus (Fig. 5.14):

$$G_{12} = \max \{ G_{ult}, G_{12_{Init.}}^j \cdot e^{-\lambda(\gamma-\gamma_0)} - \lambda(\gamma-\gamma_0) G_{12_{Init.}}^j \cdot e^{-\lambda(\gamma-\gamma_0)} \} \quad (5.51)$$

where  $\gamma$  is the shear strain (-) and  $\gamma_0 = G_{12_{Init.}}^j / S_o$ . Thus, the corresponding shear stresses at each stage of shear response is calculated as:

$$\tau_{12} = \gamma_{12} \cdot G_{12} \cdot (1 - d_3) \quad (5.52)$$

The shear damage variable,  $d_3$  is propagated in two steps as shown in Fig. 5.14. The first step occurs as soon as the shear load exceeds the linear elastic regime (namely,  $\gamma_0$  to  $\gamma_C$ ) and the second in the linear region of lower modulus (namely,  $\gamma_C$  to  $\gamma_{max}$ ). The ultimate shear strain  $\gamma_{max}$  is expressed as a function of  $\gamma_C$  through the relation:  $\gamma_{max} = fact_{12} \cdot \gamma_C$ .  $fact_{12}$  is a user-defined parameter which actually associates the energy with the shear component once  $\gamma_C$  is reached, and it is typically greater than  $G_{II_{CT}}$ , the fracture toughness in transverse tension. When this occurs a rapid stress reset is applied to the shear component which, however, can cause numerical instabilities. Therefore, a minimum  $fact_{12} = 1.05$  is applied. The damage variable is expressed through Eq. 5.53 and is shown in Fig. 5.14.

$$d_3 = 1 - \left| \frac{\tau_{12}}{\gamma_{12} \cdot G_{12}} \right| \quad (5.53)$$

#### 5.5.4 Smearing methodology

In this work, a smearing cracking methodology is implemented to model fracture, in which the stored energy of a unit volume is related to the fracture toughness, to alleviate mesh dependency and overcome strain localisation. This method requires an additional length parameter in order to achieve a constant energy release per unit area of crack generated [195, 196] regardless the

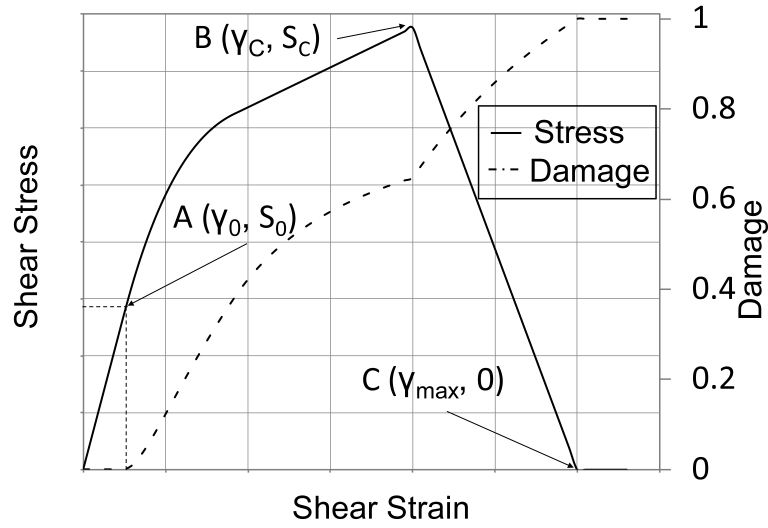


Figure 5.14: *Shear stress-strain behaviour and damage index evolution.*

element dimensions. The limitations of this approach as described by other studies [197, 198] are acknowledged by the author. For instance, the smeared formulation is effectively applied in structured meshes which are not recommended for complex geometries with discontinuities, viscous regularisation is used to obtain mesh objectivity [197] while crack growth direction can only be parallel to one of the element edges, which imposes limitations to predict damage evolution in arbitrary directions [158, 199]. Though, in the present study the simulations are performed on rectangular specimens, which can be modelled effectively with structured meshes, and the loading scenarios are not expected to form complex failure patterns. The total specific energy,  $E_f$  can be expressed as the sum of the elastic energy,  $E_e$  and the propagation energy,  $E_p$  as illustrated in Fig. 5.13. The specific energy dissipated is the area under the stress-strain curve and is defined as:

$$E_f = \int_0^\infty \sigma d\epsilon \quad (5.54)$$

The fracture energy,  $G_c$  is a material property for each failure mode and can be obtained from:

$$G_c = \int_0^\infty \sigma d\delta \quad (5.55)$$

Total specific energy within an element of volume  $V_e$  and fracture area  $A_e$  can be coupled with the fracture energy as:  $E_f \cdot V_e = G_c \cdot A_e$ . Introducing a characteristic length for an element

we have:  $l_x = A/l_1$ , where  $l_1$  is the longest length of the element area,  $A$ . Hence expressing Eq. 5.54 as:  $E_f = 1/2 \cdot \sigma_{0,i} \cdot \epsilon_{max,i}$ , the maximum element size that can be used in the mesh is defined by:

$$l_x < 2 \cdot \frac{G_c \cdot E}{\sigma_c^2} \quad (5.56)$$

where,  $l_x$  is the characteristic element length,  $E$  is the stiffness and  $\sigma_c$  is the stress at damage initiation (MPa).

## 5.6 Exponential Law - Non-Linear damage evolution

Although the bi-linear stress model, presented in Subsection 5.5.2, is widely accepted as a valid simple approach to model damage propagation which incorporates fracture toughness as the governing parameter for material degradation, it is mainly suitable for cases characterised by unstable damage propagation. In cases such as CT tests where the fracture toughness values for initiation and propagation significantly differ, with the first being essentially smaller than the latter, a large stiffness degradation can be assumed just after crack initiation while the stiffness reduction due to crack propagation is observed to be less steep.

Previous studies attempted to model the unloading response with a bi-linear law [200], in which the softening behaviour was described by two different linear laws, and reported considerably more realistic results than a linear unloading. However, in the present work, an exponential unloading law, based on F. Ehrich's [135] idea, is presented and implemented. The law accounts the differences for fracture toughness of initiation and propagation, and models the damage propagation of fibre tension failure, the dominant failure mechanism of a CT test case. This law can adopt different unloading formulations through parameter  $\omega$ , as described by Eq. 5.57 and presented in Fig. 5.15. As it can be seen in Fig. 5.15, the magnitude of parameter  $\omega$  governs the type of the unloading path, namely, for  $\omega < 0$ : concave,  $\omega = 0$ : linear and  $\omega > 0$ : convex.

$$\sigma(\epsilon_{11}) = \frac{\exp\left(\frac{(\epsilon_{o,11}-\epsilon_{11}) \cdot \omega}{\epsilon_{max,11}-\epsilon_{o,11}}\right) - 1}{\exp(\omega) - 1} \cdot \sigma_{o,11} \quad (5.57)$$

Hence, combining Eqs. 5.57 and 5.41 the damage variable can be rewritten as a function of strain and receive the form of Eq. 5.58.

$$d(\epsilon_{11}) = \frac{\epsilon_{o,11} \left[ 1 - \exp\left(\frac{(\epsilon_{o,11} - \epsilon_{11}) \cdot \omega}{\epsilon_{max,11} - \epsilon_{o,11}}\right) \right] + \epsilon_{11} \cdot [\exp(\omega) - 1]}{\epsilon_{11} \cdot [\exp(\omega) - 1]} \quad (5.58)$$

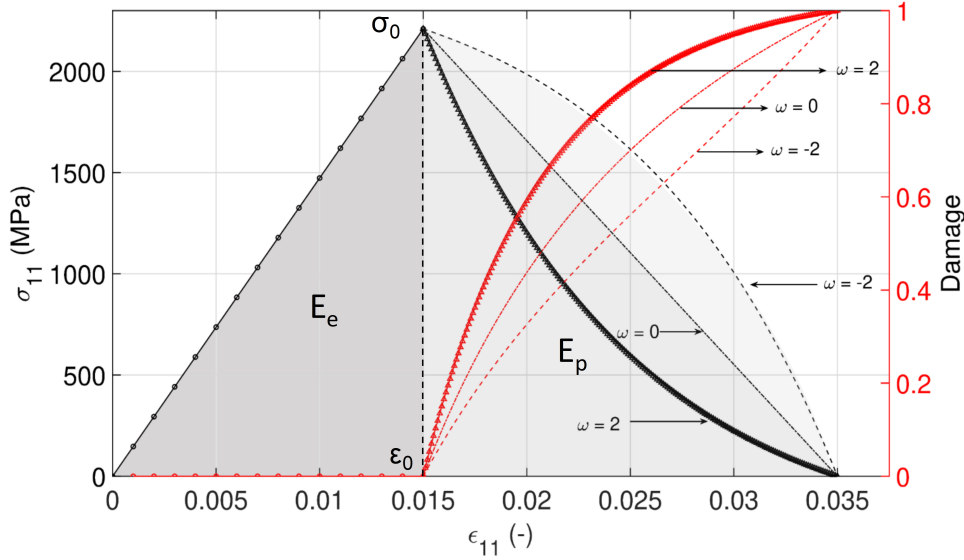


Figure 5.15: *Exponential unloading stress response for different values of  $\omega$  and the corresponding damage evolution.*

In Fig. 5.16 the values of  $\epsilon_o$  and  $\epsilon_{max}$  are fixed, hence the dissipated energy is dependant solely on  $\omega$ . The total strain energy is:

$$E_f = E_e + E_p = \int_0^{\epsilon_{o,11}} \sigma d\epsilon + \int_{\epsilon_{o,11}}^{\epsilon_{max,11}} \sigma d\epsilon \quad (5.59)$$

where the formulation of  $E_e$  coincides with the one for the bilinear law:

$$E_e = \int_0^{\epsilon_{o,11}} \sigma_{11} d\epsilon = \frac{1}{2} \cdot \sigma_{o,11} \cdot \epsilon_{o,11} \quad (5.60)$$

and the energy during unloading,  $E_p$  is the integration of Eq. 5.57:

$$E_p = \int_{\epsilon_{o,11}}^{\epsilon_{max}} \sigma d\epsilon = \frac{\sigma_{o,11}}{\exp(\omega) - 1} \cdot (\epsilon_{max,11} - \epsilon_{o,11}) \cdot \left[ \frac{1 - \exp(-\omega)}{\omega} - 1 \right] \quad (5.61)$$

Hence, the total strain energy is:

$$E_f = E_e + E_p = \frac{1}{2} \sigma_{o,11} \cdot \epsilon_{o,11} + \frac{\sigma_{o,11}}{\exp(\omega) - 1} \cdot (\epsilon_{max,11} - \epsilon_{o,11}) \cdot \left[ \frac{1 - \exp(-\omega)}{\omega} - 1 \right] \quad (5.62)$$

However, the total strain energy has to equal also  $G_c/l_x$ , where  $G_c$  is the fracture toughness in fibre tension ( $G_{ICT}^{0^\circ}$ ). Hence, the calculation of the strain at ultimate failure with regards to the fracture toughness is expressed through:

$$\epsilon_{max,11} = \epsilon_{o,11} + \frac{\frac{G_{ICT}^{0^\circ}}{l_x} - 1/2 \cdot \sigma_{o,11} \cdot \epsilon_{o,11}}{\frac{\sigma_{o,11}}{\exp(\omega) - 1} \cdot \left[ \frac{1 - \exp(-\omega)}{\omega} - 1 \right]} \quad (5.63)$$

The summarised response of the described non-linear damage evolution method is shown in Fig. 5.16, for varying values of  $G_{ICT}^{0^\circ}$  and  $\omega$ , while maintaining the characteristic element length,  $l_x$  constant. As it was expected  $\epsilon_{max,11}$  is increasing as  $G_{ICT}^{0^\circ}$  increases, but it is also proportional to  $\omega$ . This approach could also be easily extended to the other loading directions.

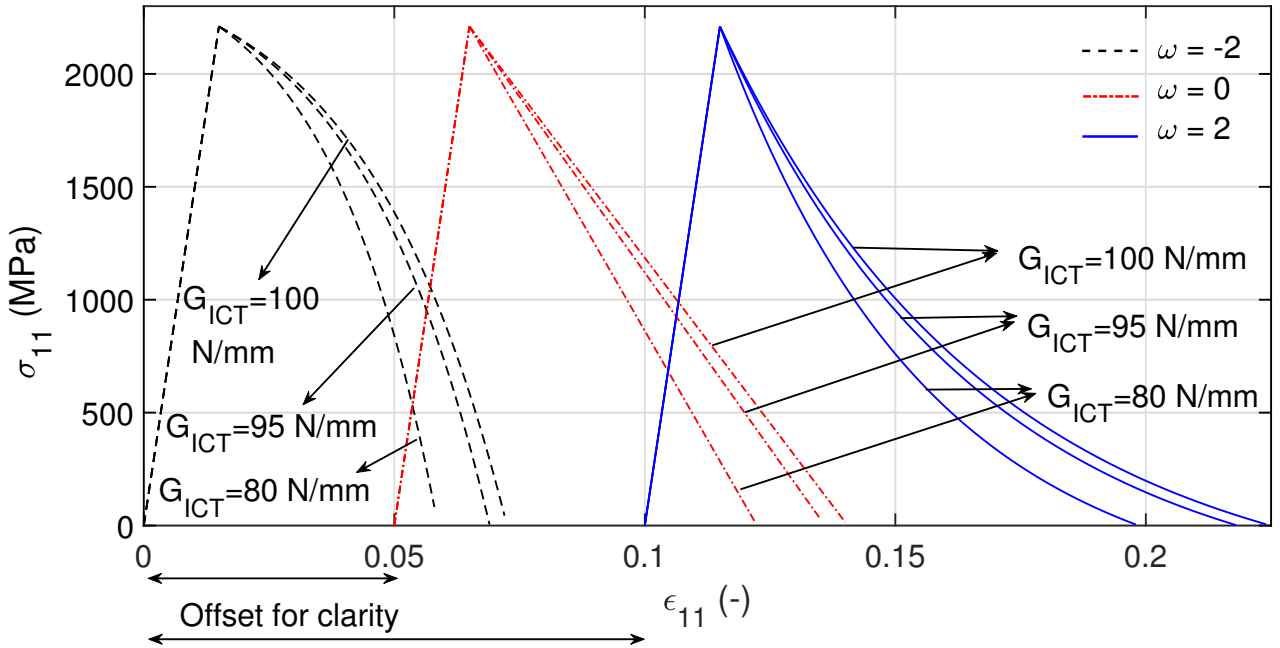


Figure 5.16: *Exponential unloading stress response for different values of  $\omega$  in conjunction to different values of  $G_{ICT}$ .*

## 5.7 Interactive damage propagation

Most continuous damage mechanics projects treat transverse tensile and shear failures independently when regards to damage propagation modelling. In this work, it is proposed to extend the interaction between transverse tension and shear stresses of the initiation damage process to damage propagation, since the author believes that this interaction does not stop with damage onset. Thus, a power law based on the maximum strain criteria for transverse tension and shear loading is assumed and forms the initiation and final failure envelopes which are defined by an ellipse ranging between the failure strain values for pure loading conditions (Eq. 5.64):

$$\left( \frac{\epsilon_{i,22}}{\epsilon_{22,max}} \right)^2 + \left( \frac{\gamma}{\gamma_{max}} \right)^2 = 1 \quad (5.64)$$

The maximum strains for pure modes are computed as follows:

$$\epsilon_{max,22} = 2 \cdot \frac{G_{ICT}^{90^\circ}}{\sigma_{0,22} \cdot l_x}, \quad \gamma_{max} = fact_{12} \cdot \gamma_C \quad (5.65)$$

where  $\sigma_{0,22}^+$  is the transverse tensile strength at failure onset. The parameter  $fact_{12}$  specifies the ratio between shear strain at failure onset ( $\gamma_C$ ) and complete failure ( $\gamma_{max}$ ). It in fact correlates  $G_{ICT}^{90^\circ}$  with the energy release rate during in-plane shear. Namely, for  $fact_{12} = 1$ , the energy release rate due to the in-plane shear is taken to be equal to  $G_{ICT}^{90^\circ}$  and hence  $\gamma_{max}$  is defined accordingly. However, if non-linear shear behaviour is modelled, the energy associated with the shear component once  $\gamma_C$  is reached, is typically greater than  $G_{ICT}^{90^\circ}$  and a rapid stress reset will be applied to the shear component. To avoid numerical issues caused by instantaneous stress reset, a minimum  $fact_{12} = 1.05$  is recommended, although the appropriate value is defined after performing an one element analysis in in-plane shear and compared against the experimental data, as it was later performed in Chapter 6. The interactive damage propagation is described by the means of strain values at initiation A ( $\epsilon_{d,22}, \gamma_d$ ), at final failure C ( $\epsilon_{f,22}, \gamma_f$ ), as well as at the current strain state B ( $\epsilon_{i,22}, \gamma_i$ ). The calculation of strains at points A and C is based on the current strain state (point B), and is recalculated at every time step to account load redistribution and changes in damage ratio. Damage is assumed to radially propagate along

a straight line through the origin and the current strain state, which is expressed as a linear function in the form of  $\gamma = \gamma_i/\epsilon_i \cdot \epsilon$ . Hence, the corresponding shear strains for both damage initiation Eq. 5.66a) and ultimate failure Eq. 5.66b) are:

$$\gamma_d = \sqrt{\gamma_c^2 \cdot \left(1 - \frac{\epsilon_{i,22}^2}{\epsilon_{o,22}^2}\right)} \quad (a), \gamma_f = \sqrt{\gamma_{max}^2 \cdot \left(1 - \frac{\epsilon_{i,22}^2}{\epsilon_{max,22}^2}\right)} \quad (b) \quad (5.66)$$

Intersecting Eqs. 5.66a) and b) with the damage propagation linear function, the predicted initiation and failure strains are obtained as follows:

$$\epsilon_d = \frac{\epsilon_{o,22} \cdot \gamma_c}{\sqrt{\epsilon_{o,22}^2 \cdot \gamma_i + \gamma_c \cdot \epsilon_{i,22}}} \cdot \epsilon_{i,22}, \epsilon_f = \frac{\epsilon_{max,22} \cdot \gamma_{max}}{\sqrt{\epsilon_{max,22}^2 \cdot \gamma_i + \gamma_{max} \cdot \epsilon_{i,22}}} \cdot \epsilon_{i,22} \quad (5.67)$$

Fig. 5.17 depicts the initiation and propagation failure envelopes as well as the developed damage indices for combinations of the transverse tensile and shear strain values. A linear unloading is assumed between damage initiation and ultimate failure of the material. This interaction criterion is considered to be important for: a) the CT case study since the 90° layers of the specimens (layers whose fibre orientation is perpendicular to load direction) are likely to exhibit matrix dominated failure as a result of the combined transverse tension and shear stresses and b) the LVI cases where due to the geometric symmetry, the matrix dominated failures of  $\pm 45^\circ$  layers couple transverse tension and in-plane shear failure.

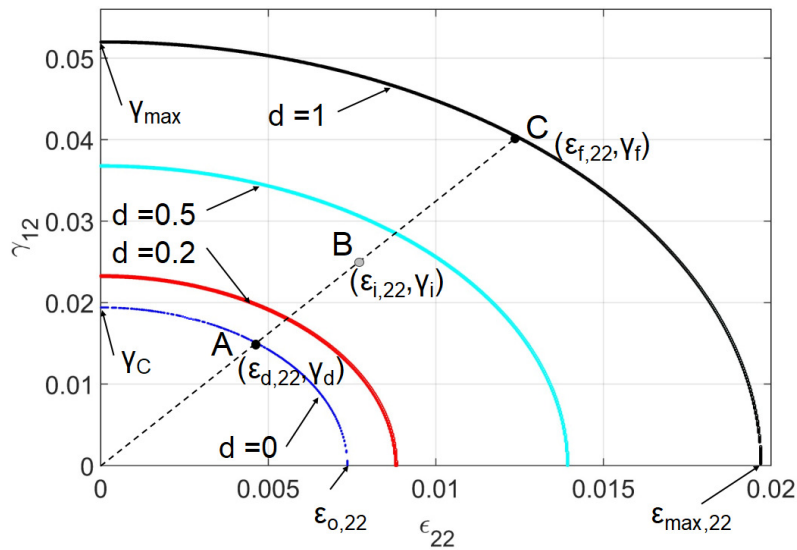


Figure 5.17: Interactive damage propagation through the origin.



# Chapter 6

## Constitutive Model Validation and FE Implementation

### 6.1 Introduction

The analytical formulations and the constitutive failure criteria presented in Section 4 were introduced to Simulia Abaqus/Explicit as a material model through a VUMAT subroutine and validated against the test results presented in Chapter 3. Due to the non-linearities (e.g. in-plane shear, matrix failure modes) of the material constitutive formulation as well as due to the modelling of contacts (i.e. cohesive surfaces), where any change in contact is also extremely non-linear, the explicit analysis is opted so that a converged solution is ensured. On the other hand, the implicit algorithm for the numerical cases of this study would require many more iterations to solve such problems and more importantly solution convergence issues will appear.

The present chapter aims to compare the proposed failure criteria for NCF composites against the default failure criteria of the original version of the energy-based damage model, through one element analyses. Particular focus is given on the influence of the in-situ strengths in failure onset prediction as incorporated in matrix tension and compression. Furthermore, a numerical study to validate the interlaminar properties as obtained from analytical formulations

is performed by conducting a series of finite element analyses of DCB and 4pt-ENF cases.

## 6.2 One element Analysis

The one element analyses were carried out in tension, in-plane shear and compression on an element of size 10 mm x 10 mm x 2 mm. The lay-up configurations were matching the tested specimens for each loading case. Since the material used was biaxial NCF, it has been decided instead of homogenising the NCF blanket, to model each layer of the NCF blanket separately with additional integration points, meaning that the orientations of the individual layers within the NCF blanket are taken into consideration. Thus, the failure of each layer can be predicted. The constitutive model has five failure flag options, which specify which damage component(s) upon reaching unity will cause material point removal:

- 1. Fibre failure and non-zero matrix damage; or matrix failure and non-zero fibre damage (tension).
- 2. Both fibre and matrix failure
- 3. In-plane shear
- 4. Fibre failure or matrix failure

For each FE analysis the appropriate failure flag was opted.

### 6.2.1 Material properties and implementation

The VUMAT material card requires at layer level a total of twenty-eight user-defined input parameters, including the material properties (Tab. 6.1). The in-plane elastic properties (i.e.,  $E_{11}$ ,  $E_{22}$ ,  $G_{12_{init}}^j$ ,  $\nu_{12}$ ) and well as the respective strength properties ( $X_T$  and  $X_C$ ) are calculated directly from the micromechanical properties of fibres, resin and stitches and the corresponding volume fractions as presented in Section 5.2. The out-of-plane properties,  $G_{13}$ ,  $\nu_{13}$ ,  $Z_T$ , were obtained from literature sources for the same material system [130], whereas the fibre and matrix

compression debris strengths,  $X_{CF}$  and  $Y_{CF}$  were considered as measured in T. Katafiasz's PhD thesis [142]. As debris strength is defined the residual strength of the resin or crush compressive material.

| Properties                                                                                | Description                                                 |
|-------------------------------------------------------------------------------------------|-------------------------------------------------------------|
| $E_{11}, E_{22}, G_{12_{Init.}}^j, G_{13}, \nu_{12}, \nu_{13}$                            | Elastic properties and Poisson's ratios                     |
| $X_T, X_C, Z_T, X_{CF}, Y_{CF}$                                                           | Strength properties                                         |
| $G_{ICT}^{0^\circ}, G_{ICT}^{90^\circ}, G_{ICC}^{0^\circ}, G_{ICC}^{90^\circ}, fact_{12}$ | Fracture toughness                                          |
| $\lambda, S_o, G_{Ult}$                                                                   | Shear parameters                                            |
| $t_{layer}, G_{IC}, G_{IIC}, \beta, IS\_factor$                                           | In-situ strength parameters                                 |
| $\theta_I, \phi_I$                                                                        | Angular fibre misalignment                                  |
| $\alpha_o, \omega$                                                                        | Fracture plane angle and exponential unloading coefficient. |

Table 6.1: *Summary of input material properties and parameters for the constitutive model.*

Moreover, whilst the fracture toughness in fibre tension,  $G_{ICT}^{0^\circ}$  is obtained from the CT tests of Section 4.8, the fracture toughness in fibre compression, and matrix tension and compression are received from Gutkin's et al. work [54]. Also, *factor12* specifies the ratio between shear strain at ultimate shear stress and at complete failure. Hence, for the non-linear in-plane shear behaviour the precise value of *factor12* is found after an iterative one element simulations. With regards to the in-plane shear parameters, the non-linearity constant,  $\lambda$  which governs how rapid the transition from  $G_{12_{Init.}}^j$  to  $G_{Ult}$  occurs, together with  $S_o$  and  $G_{Ult}$  are directly extracted from the in-plane shear tests.

The knock-down factor's calculation is based on the mean initial in-plane,  $\phi_I$  and out-of plane,  $\theta_I$  fibre misalignments are obtained from optical microscopy image analysis as described in Chapter 3 and they are necessary for the calculation of the longitudinal stiffness knock-down factor and fibre compression failure criterion.

As stated in Section 5.2, the in-situ strengths in transverse tension and compression and longitudinal and transverse shear are defined by the nominal thickness of the layer,  $t_{layer}$ , the intralaminar fracture toughness in Mode I and Mode II, and parameter  $\beta$ . Although, Dvorak and Camanho's formulation originally requires the intralaminar fracture toughness values, the author consider that it is a fair assumption to use the interlaminar fracture values instead, since as suggested by Pinho et al. [201] and Catalanotti [189] in the absence of experimental data and

due to the similarity of the fracture modes for the same material systems, the intralaminar fracture toughness can be approximated with the interlaminar for Mode I and Mode II. As a result of this, for the calculation of the in-situ strengths the interlaminar fracture toughness ( $G_{IC}$ ,  $G_{IIC}$ ) were introduced into the analytical formulation. The  $\beta$  factor is also defined through an iterative one element analysis simulation. Furthermore, the  $IS_{Factor}$  works as a switch in the source code, where the user can select the values of 0, 1 or 2. The value of 0 corresponds to UD-NCF laminate, 1 for thin outer layer and 2 for thin embedded layer.

The constitutive failure model requires also the input of fracture plane angle for pure compression,  $\alpha_0$  which is found to be  $40^\circ$  according to fractography. In Tabs. B.6 and B.7 in Appendix B.1.3, the material properties of T700S/MTM57 and T700S/SD8100/SR8824 systems are summarised respectively for each loading case, since the specimens of each loading scenario were cut from separate laminates with different volume fractions of the constituents.

In the one element analyses, a continuum shell element with reduced integration (SC8R) was used, which, however, requires the allocation of the through-the-thickness "transverse shear stiffness", which were not physically measured. Hence, the transverse shear stiffness was directly defined by three stiffness components as proposed by Ankersen [191]:

$$K_{11} = \sum_{i=1}^n \frac{5}{6} \cdot E_{i,66} \cdot t_i, \quad K_{22} = \sum_{i=1}^n \frac{5}{6} \cdot E_{i,55} \cdot t_i, \quad \& \quad K_{12} = \sum_{i=1}^n \frac{5}{6} \cdot E_{i,56} \cdot t_i \quad (6.1)$$

where,  $K_{11}$  is the transverse shear stiffness in the  $0^\circ$  layer (reference direction),  $K_{22}$  is the transverse shear stiffness in the  $90^\circ$  layer,  $K_{12}$  is the coupling component. The  $K_{ii}$  values are found by summation through  $n$  layers of the laminate with layer thickness  $t$  and layer modulus,  $E$  with reference to the laminate reference direction. For each layer angle,  $E_{55}$ ,  $E_{66}$  and  $E_{56}$  are found to by transformation of the layer values  $E'$  such that:

$$\begin{aligned}
E_{55} &= \cos^2(\theta) \cdot E'_{55} + \sin^2(\theta) \cdot E'_{66}, \\
E_{66} &= \cos^2(\theta) \cdot E'_{66} + \sin^2(\theta) \cdot E'_{55}, \\
E_{56} &= \cos(\theta) \cdot \sin(\theta) \cdot (E'_{55} - E'_{66})
\end{aligned} \tag{6.2}$$

where,  $E'_{55} = G_{23}$ , is the transverse shear modulus normal to the fibre direction (MPa) and  $E'_{66} = G_{13}$ . The value of  $G_{23}$  is assumed to be equal to resin stiffness since the transverse shear modulus is significantly matrix-dominated.

### 6.2.2 Tension

The first load case scenario concerns the longitudinal tensile loading for the  $[0^\circ/90^\circ]_{4S}$  lay-up. Through this analysis, the original failure model (Eq. 5.33) adopting the transverse tensile strength as suggested by Eq. 6.3 was compared with the proposed NCF failure model (Eq. 5.34) which includes the in-situ transverse strengths.

$$Y_T = (1 + V_M \cdot E_M/E_{F22} + V_{Pes} \cdot E_{Pes} \cdot \sin(45^\circ)/E_{F22}) \cdot Y_{Tb} \tag{6.3}$$

The tensile load is expressed in terms of displacement rate ( $U_X$ , mm/min) equal to 2 mm/sec and it is applied in nodes 5, 6, 7 and 8. The boundary conditions of the element are presented in Fig. 6.1.

The material properties used are summarised in Tab. B.6 in Appendix B.1.3 for the tensile loading scenario, while the results are correlated against the test results of Section 4.3. The original failure model was denoted as "Eq. 5.32 (W/O IS)", while the modified version as "Eq. 5.33 - IS", which implies that the latter takes into account the in-situ strengths.

In Fig. 6.2, the behaviour of the  $[0^\circ/90^\circ]_{4S}$  configurations is presented. Although both one element analyses predicted very accurately the ultimate tensile failure load, since the same fibre tensile criterion is used, it can be clearly seen that for the analysis omitting the in-situ

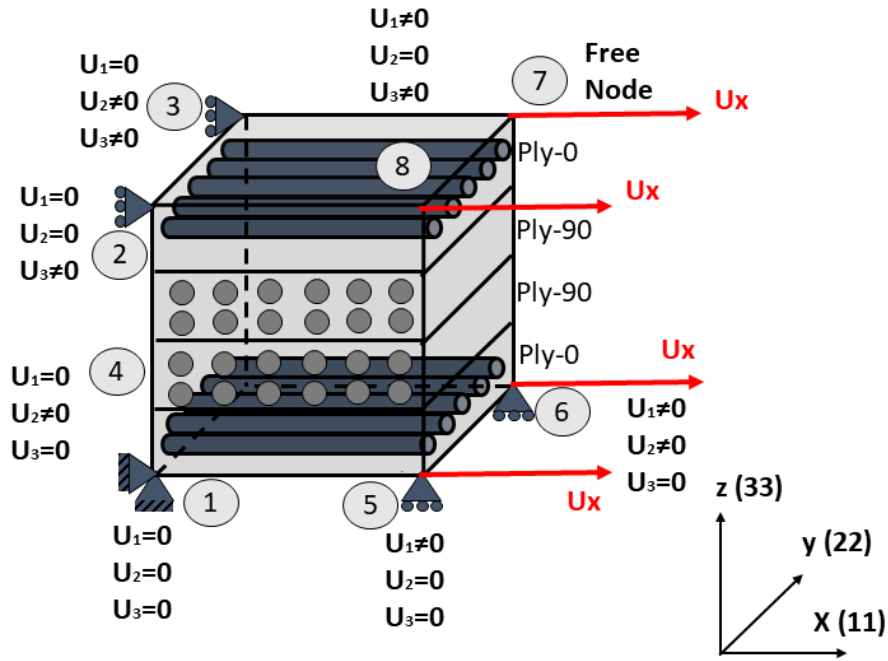


Figure 6.1: Boundary conditions of a single element in tension (model not scaled).

tensile strengths (W/O IS) the 90° layers of the configuration failed all simultaneously in tension at 0.015 strain, whereas for the analysis including the in-situ strengths, the respective failure occurred gradually at larger strains (0.017) due to the enhanced transverse tensile strength, which appears to be much closer to the actual behaviour of the material.

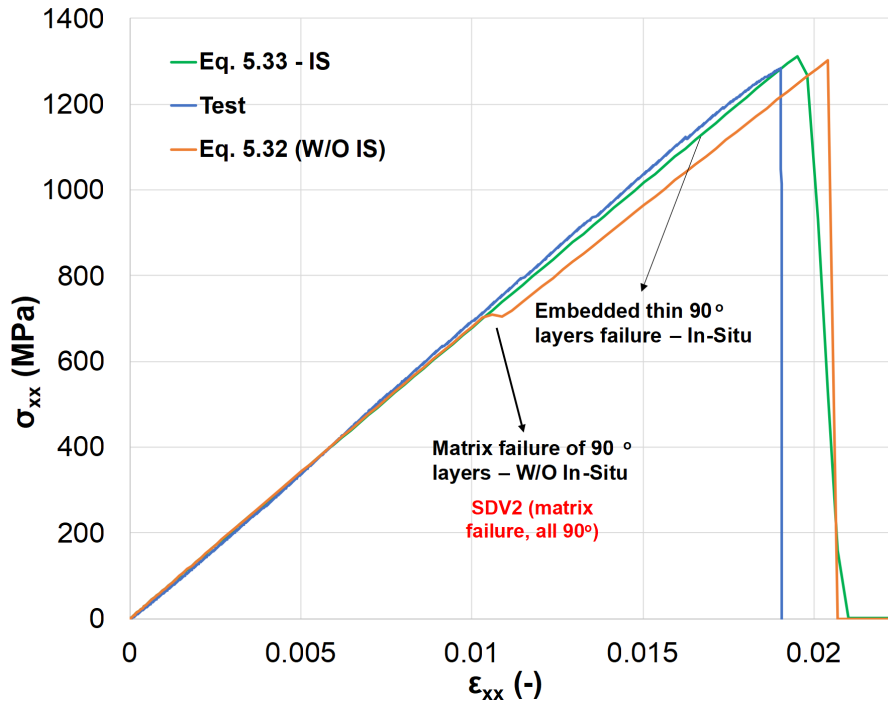


Figure 6.2: One element analysis in tension for a  $[0^\circ/90^\circ]_{4S}$  lay-up.

### 6.2.3 Shear

Aside from the transverse tensile case, the in-situ strengths have the same, if not greater, significance in in-plane shear. Under shear load, the NCF layer is governed by the matrix shear strength. As for the shear modulus calculation, the influence of the stitches is introduced in the  $+45^\circ$  layer. Hence, we have:

$$S_{C_{+45}} = (1 + G_M/E_{b22} + V_{Pes} \cdot E_{Pes}/E_{b22}) \cdot S_{C_b} \quad (6.4)$$

$$S_{C_{-45}} = (1 + G_M/E_{b22}) \cdot S_{C_b} \quad (6.5)$$

where,  $S_{C_b}$  = Shear strength of the fibre bundle in (MPa) as obtained by Eq. 6.6.

$$S_{C_b} = \frac{G_M}{SRF_{Shear}} \quad (6.6)$$

Similar to the transverse strength calculation, the shear strength of a bundle is affected by a Strength Reduction Factor (SRF) due to stress concentration at the fibre-matrix interface. Thereafter, from Eq. 6.7 the value of  $SRF_{Shear}$  can be obtained.

$$SRF_{Shear} = \frac{1 - V_{Mb} \cdot (1 - G_M/G_{b12})}{1 - \sqrt{\frac{4 \cdot V_{Mb}}{\pi}} \cdot (1 - G_M/G_{b12})} \quad (6.7)$$

In this case, the failure criterion of Eq. 5.33 (Eq. 5.33 (W/O IS + Stitches)) which adopts the shear and matrix tension strengths from Eqs. 6.4, 6.5 and Eq. 6.3 respectively, is compared against the proposed NCF failure criterion (Eq. 5.34 - IS) and against criterion of Eq. 5.33 in which the stitch parameters,  $V_{Pes} \cdot E_{Pes}$  are not included in  $Y_T$  and  $S_C$  (Eq. 5.33(W/O IS + No Stitches)). In order to enforce a pure shear deformed shape for the one element model, the displacement loading boundary conditions were applied as seen in Figs. 6.3 and 6.4. It can be observed that a distributed loading methodology was used to minimise numerical errors due to localisation. Namely, in three pairs of nodes (3 and 4, 6 and 7, 5 and 8), explicit displacement rates in x- and y-directions are applied, to ensure pure shear deformation.

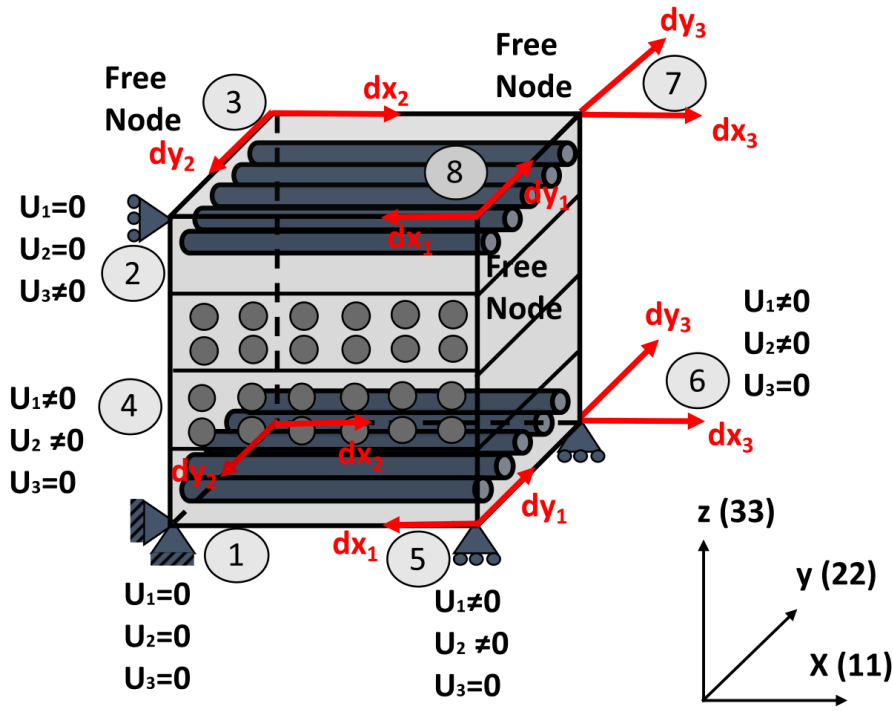


Figure 6.3: Boundary conditions of a single element in in-plane shear (model not scaled).

The displacement rate calculations for each pair of nodes were based on the theoretical approach, which considers that shear deformation will occur solely with the change of angle  $\theta$  (Fig. 6.4), while assuming that:  $l_{x0} = l_{y0} = l_0 = 8 \text{ mm}$ ,  $\alpha_1 = \alpha_2$  and  $\alpha_1 + \alpha_2 + \theta = 90^\circ$ . Hence,

$$\sin \alpha_1 = dx_1/l_{x0}, \quad \cos \alpha_1 = (l_{x0} - dx_1)/l_{x0} \quad (6.8a)$$

$$\sin \alpha_2 = dx_2/l_{y0}, \quad \cos \alpha_2 = (l_{y0} - dy_2)/l_{y0} \quad (6.8b)$$

And then,

$$dx_1 = l_o \cdot (1 - \cos \alpha_1), \quad dy_1 = l_o \cdot \sin \alpha_1 \quad (6.9a)$$

$$dx_2 = l_o \cdot \sin \alpha_2, \quad dy_2 = l_o \cdot (1 - \cos \alpha_2) \quad (6.9b)$$

$$dx_3 = dx_1 + dx_2, \quad dy_3 = dy_1 + dy_2 \quad (6.9c)$$

So, for a range of  $\theta$  between  $90^\circ$  the displacement rates can be obtained. It is considered that  $\theta$  angle changes incrementally at a rate of  $0.1^\circ/\text{time step}$ . Figs. 6.5, 6.6 and 6.7 present the evolution of the applied displacements in x- and y- directions with regards to angle  $\theta$ .



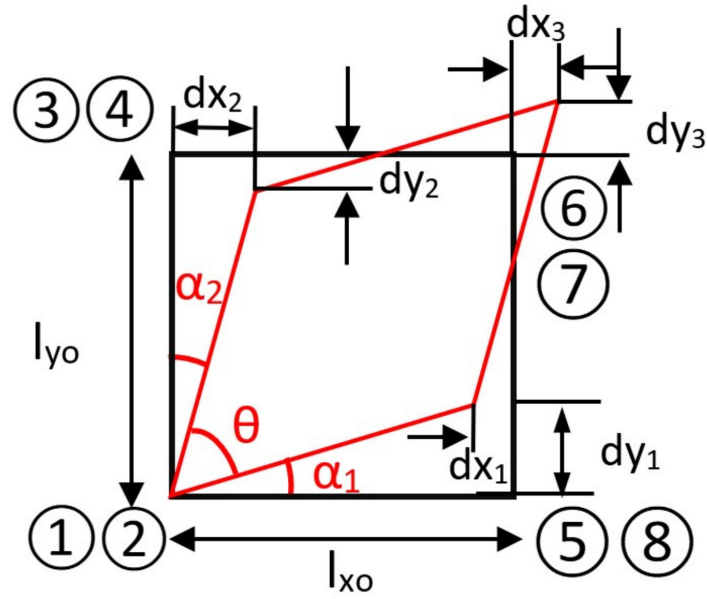


Figure 6.4: Schematic of the enforced pure shear deformed shape for the one element model (model not scaled).

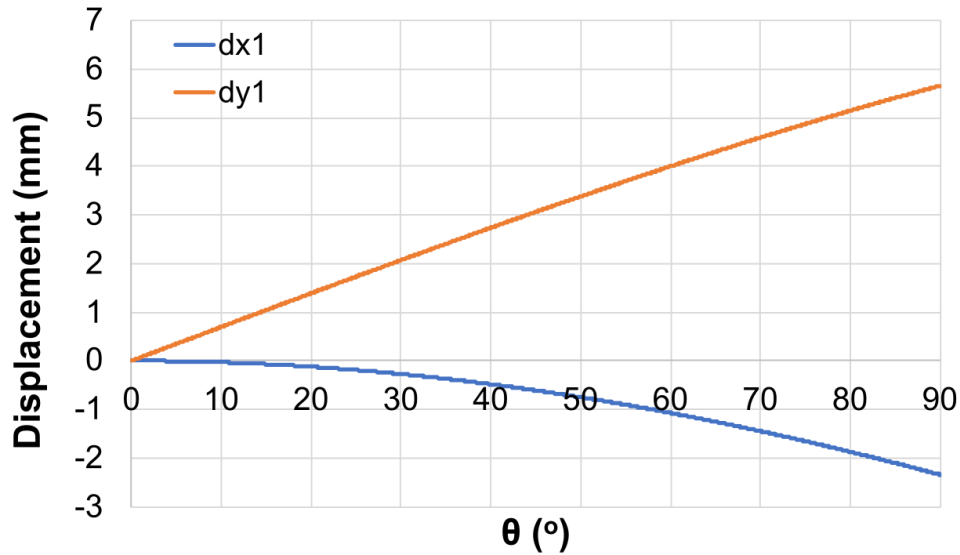


Figure 6.5: Evolution of the applied displacements in  $x$ - and  $y$ - directions with relation to angle  $\theta$  applied on nodes 5 and 8.

The material properties used, are summarised in Tab. B.6 in Appendix B.1.3 under shear column. Parameters  $\beta$  and  $\lambda$  are defined after performing iterative one element simulations in shear and were found to be equal to  $3.2e-8 \text{ MPa}^{-3}$  and 59.15 respectively. In Fig. 6.8 a comparison of the element at a fully deformed and undeformed state in the  $xy$  plane is presented, revealing its pure shear deformation. Moreover, from Fig. 6.8, it can be observed that angles  $\alpha_1$  and  $\alpha_2$  are found to be equal at  $9^\circ$  and the length of the edges of the element

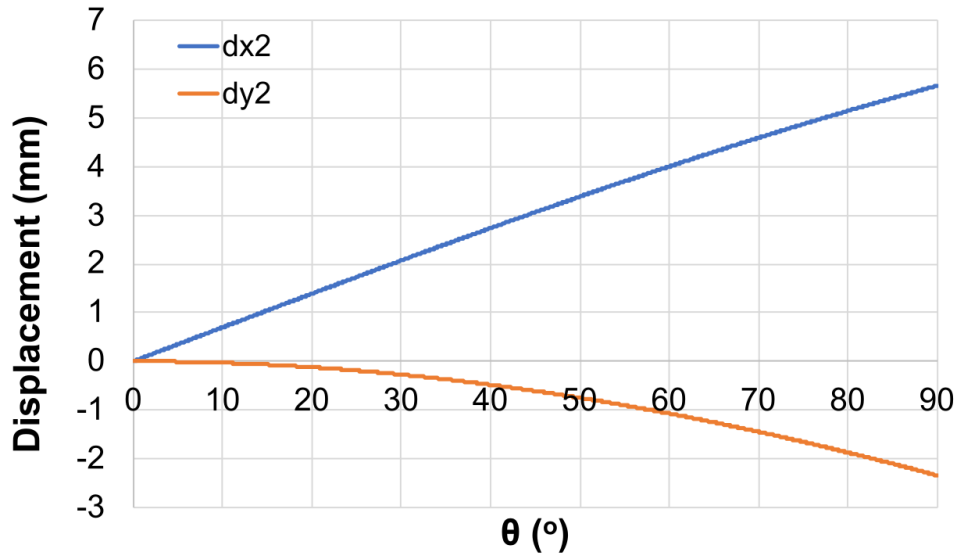


Figure 6.6: Evolution of the applied displacements in  $x$ - and  $y$ - directions with relation to angle  $\theta$  applied on nodes 3 and 4.

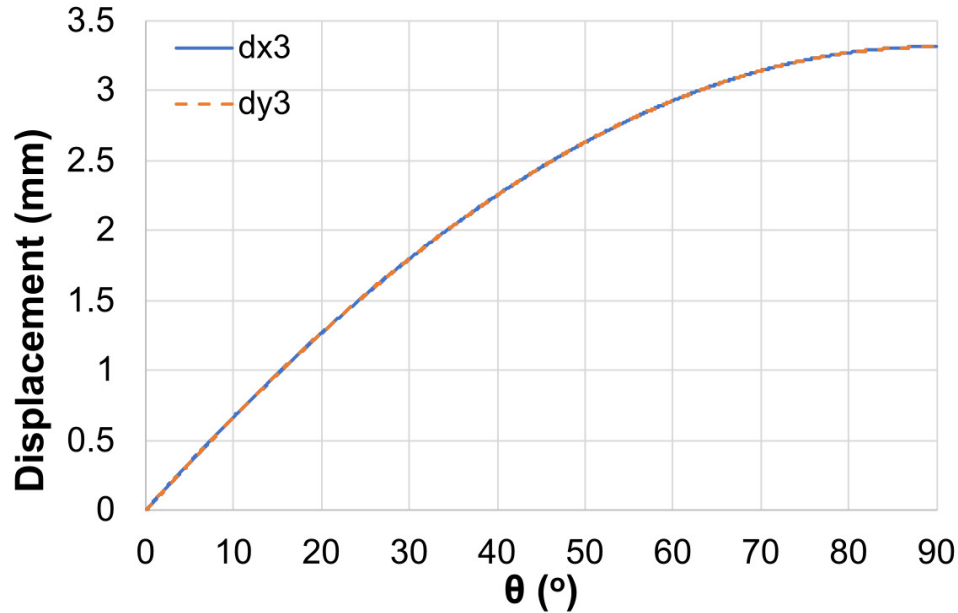


Figure 6.7: Evolution of the applied displacements in  $x$ - and  $y$ - directions with relation to angle  $\theta$  applied on nodes 6 and 7.

remained unchanged during shear deformation.

Fig. 6.9 presents the response of the one element analysis; it can be clearly observed that the numerical models "Eq. 5.32 (W/O IS + Stitches)" and "Eq. 5.32 (W/O IS + No Stitches)" predict shear failure prematurely while the proposed model with the in-situ shear strengths included, captures quite accurately the entire trend of the shear response, both in terms of shear strength and strain. The failure of the outer layers can be identified by the load drop of

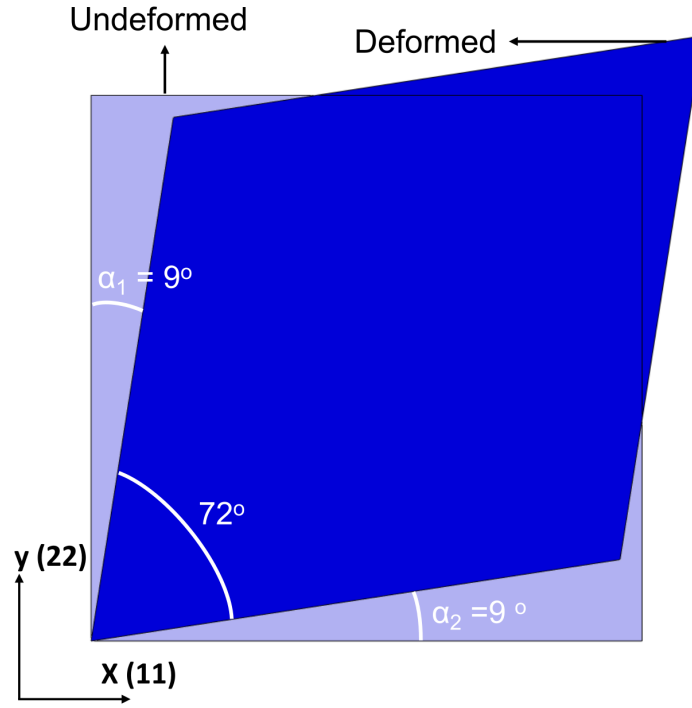


Figure 6.8: *Deformed one element under enforced in-plane shear.*

the curve at strain equal to 0.058. It is also apparent that the enhanced strengths due to the stitches ("Eq. 5.32 (W/O IS + Stitches)") have a positive contribution to predict the shear response and particular the ultimate shear failure. One limitation that can be identified in the proposed model is that, unlike Eq. 6.4, the enhanced strength of the  $+45^\circ$  layer - due to the alignment of the stitches to the loading direction - cannot be modelled explicitly. Thus, the longitudinal shear strength of the  $+45^\circ$  layer is assumed to be equal to the  $-45^\circ$  layer for thin embedded layers. A possible way to overcome this challenge is to apply a higher value of  $\beta$  in Eq. 5.13 for the  $+45^\circ$  layer than in the  $-45^\circ$ , although, during this project, a clear, direct connection between parameter  $\beta$  and polyester stitches tensile strength was not identified.

#### 6.2.4 Compression

The one element analysis in compressions aims to compare the prediction capabilities of the maximum stress criterion, which was the default criterion in fibre compression of the constitutive model, with the newly proposed failure index of Eq. 5.32 in a  $[0^\circ/90^\circ]_{4S}$  lay-up. The compressive load is expressed in terms of displacement rate ( $U_X$ ) equal to -2 mm/s and, as in tension load case, it is applied in nodes 5, 6, 7 and 8. The boundary conditions of the element

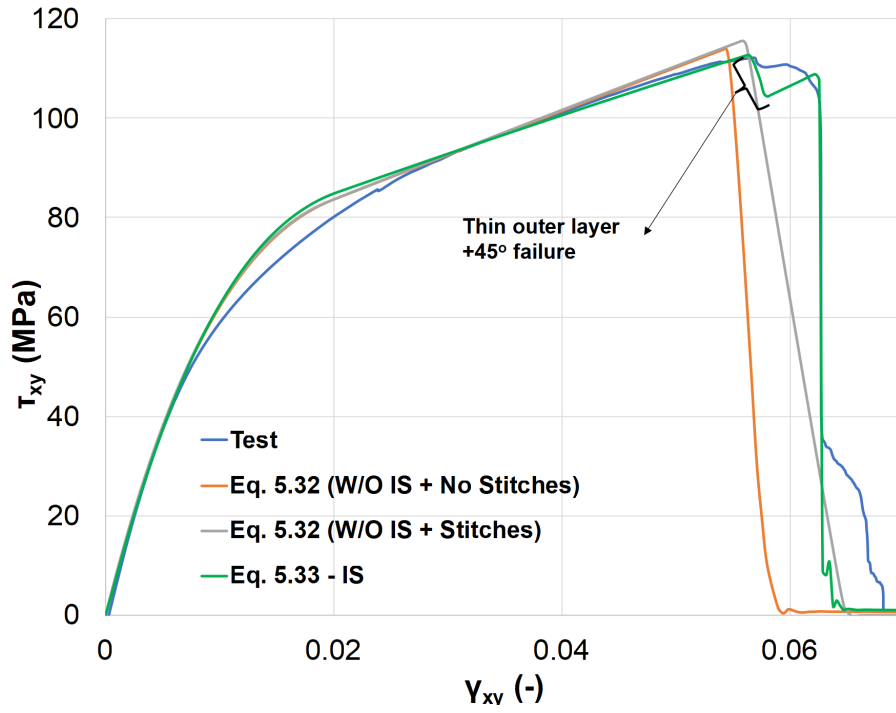


Figure 6.9: One element shear response of three different failure matrix tension criteria.

are shown in Fig. 6.10 and the same constraints with the tension scenario are applied. The material properties at layer level for this scenario were obtained from Tab. B.6 in Appendix B.1.3.

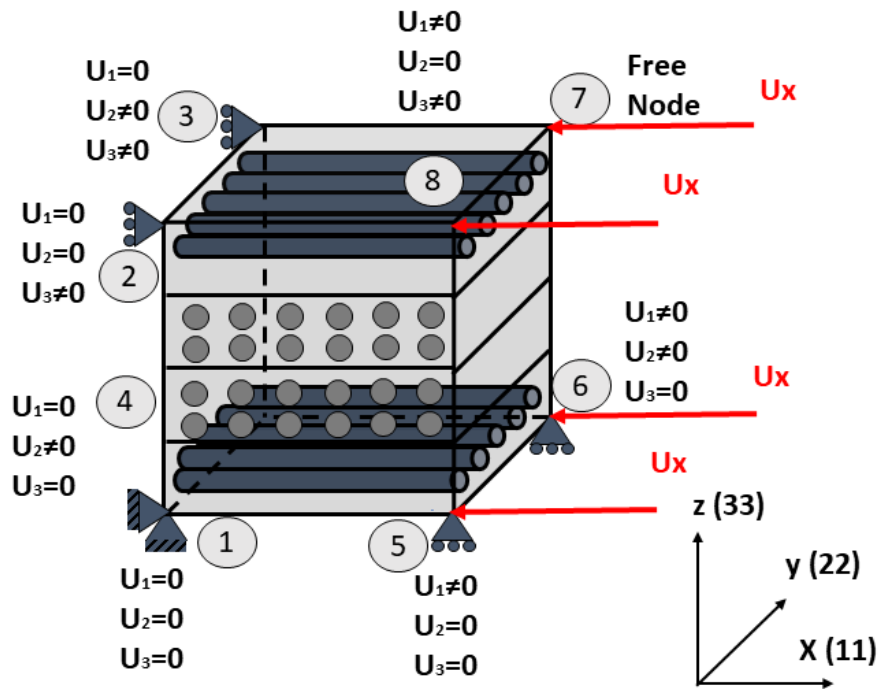


Figure 6.10: Boundary conditions of a single element in compression (model not scaled).

Fig. 6.11 describes the stress - strain behaviour from zero strain until complete failure and regard both  $0^\circ$  and  $90^\circ$  layers. The  $90^\circ$  layers are subjected to transverse compressive loading and their failure is determined by Eq.5.38. Both models correlate well with the test curve in linear elastic region (0 - 0.004) and exhibit identical response with each other. This is attributed to the fact that both models utilise the same knock-down factor formulation to obtain the effective longitudinal modulus,  $E_{11_{Eff}}$ . However, the ultimate compressive stress was overestimated by approximately 20% by the maximum stress criterion whereas for the new failure criterion the discrepancy in compressive strength is as low as 1%. The reason for this large deviation is that the maximum stress criterion assumes perfectly straight fibres without any misorientation, whereas the criterion of Eq. 5.32 accounts both in- and out-of-plane fibre misalignment. The non-linear response from strain equal to 0.006 for both analyses does not agree well with the test curve, resulting in 12% deviation of strain at failure onset. This could be attributed to the simplified boundary conditions of the one element model, in which the end-tabs are not included.

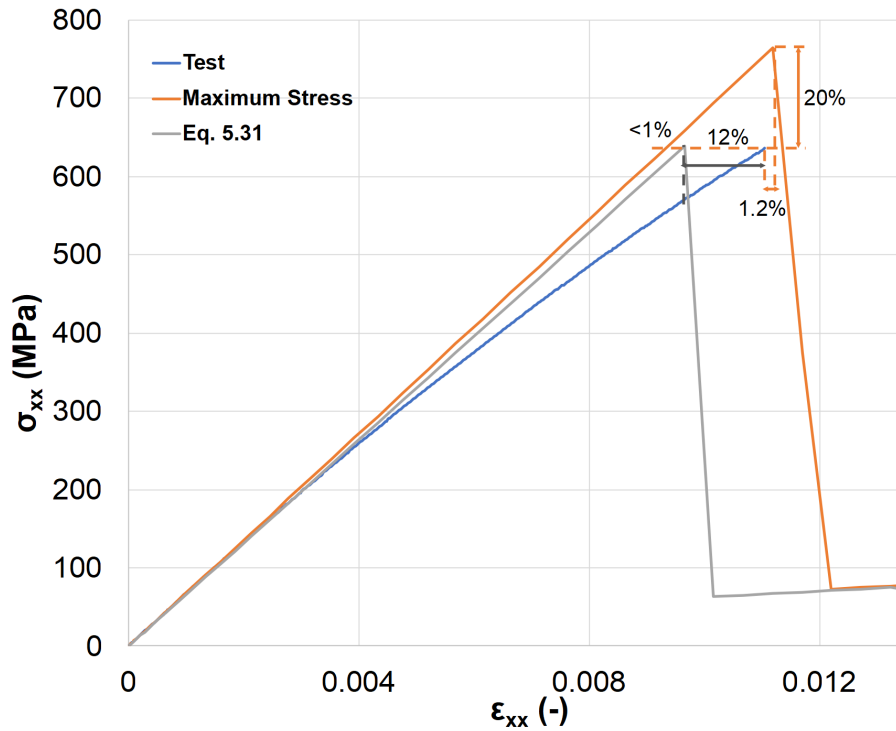


Figure 6.11: One element analysis in compression for a  $[0^\circ/90^\circ]_{4S}$  lay-up.

### 6.3 Interface Properties

Several researchers attempted to model the interlaminar failure of NCF composites by explicitly modelling the stitches however, the proposed approaches are time demanding and practically not applicable in coupon and larger scale structural components. In this work, the interaction between NCF blankets and the modelling of delamination is described by an analytical formulation which is implemented with the cohesive surfaces procedure where the behaviour of the stitches is smeared with global response of decohesion. The experimental data from Mode I - DCB and Mode II - 4pt ENF were used together with analytical calculations in order to obtain the interface properties. A bilinear power law was used for the cohesive properties (Fig.6.12), where Mode I and Mode II interlaminar toughnesses interacted together with quadratic power laws as follows for the stress-based initiation, and energy-based propagation [202].

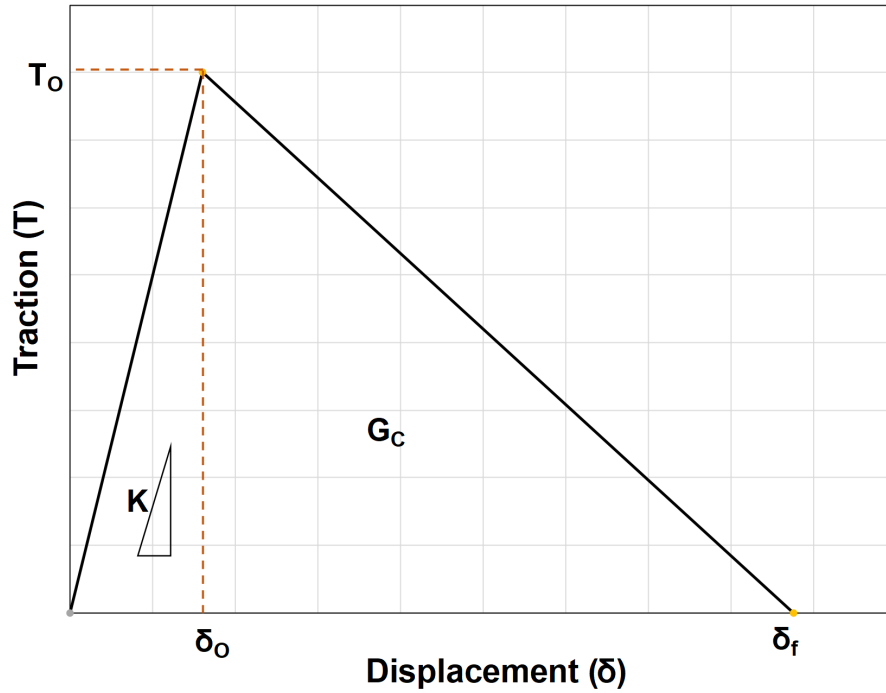


Figure 6.12: *Illustration of boundary conditions and interface properties for the DCB FE model.*

The initial response of the Cohesive Zone (CZ) is assumed to be linear until a damage initiation criterion is met. The penalty stiffness for Mode I or II of the bilinear traction-separation law is defined as [203]:

$$K_i = \frac{T_o^i}{\delta_o^i} \quad (6.10)$$

where,  $i=n$  (normal component - Mode I) or  $s$  (shear component - Mode II),  $\delta_o^i$  is critical separation for damage initiation (mm), and  $T_o^i$  is interfacial strength for Mode I or Mode II (MPa). Similarly, the relative displacement for separation,  $\delta_f$  is calculated from Eq. 6.11:

$$G_c = \frac{1}{2} \cdot \delta_f \cdot T_o^i \quad (6.11)$$

where  $G_c$  is the energy release rate of interlaminar failure and is denoted as  $G_{IC}$  for Mode I and  $G_{IIC}$  for Mode II failure respectively. The cohesive surfaces and the bulk constitutive relations of the layer define the effective elastic properties of the NCF laminate globally. Although the global deformation of the laminate can be affected by the compliance of the cohesive surfaces, their main purpose is to model delamination. Turon et al. [204], proposed the penalty stiffness for Mode I to be calculated with inequality 6.12, which ensures that the bulk material compliance is much larger than the initial compliance of the CZ. Hence in this work inequality 6.12 is implemented for the estimation of the penalty stiffness:

$$K_n \geq \alpha' \cdot \frac{E_{33}}{t/2} \quad (6.12)$$

where,  $\alpha$  is the bulk coefficient, which ensures that the compliance of the bulk material is much larger than the initial compliance of the cohesive surface. Turon [204] suggests value of 50, however, in this study the value of  $\alpha'$  was determined after performing a series of DCB FE simulations.  $E_{33}$  is the through-the-thickness modulus assumed to be equal to  $E_{22}$  and  $t$  is the thickness of Mode I specimen. With regards to the calculation of the penalty stiffness for Mode II,  $K_s$ , the proposal of Iannucci and Willows [13] is followed:

$$K_s = 2 \cdot \frac{G_{23}}{e} \quad (6.13)$$

where,  $G_{23}$  is the transverse shear modulus which for numerical applications is assumed to be

equal to the values attributed to the matrix, i.e.,  $G_M$  and  $e$  corresponds to the resin interface thickness between the NCF blankets, whose average value was obtained from optical microscope equal to 0.01 mm.

The length of the cohesive zone  $l_{cz}$  is defined as the distance from the crack tip to the point where the maximum cohesive traction is attained and can be obtained from Hillerborg's model [205]:

$$l_{cz} = E_{22} \frac{G_{I/IIIC}}{T_{I/II}^2} \quad (6.14)$$

For the FE implementation using CZM, the tractions in the cohesive zone must be represented by the finite element discretisation, hence the number of elements in the cohesive zone is:

$$N_e = \frac{l_{cz}}{l_e} \quad (6.15)$$

where,  $l_e$  is the size of the element in the direction of crack propagation. It is apparent that  $l_{cz}$  is mesh dependent and when too few elements are used to model the cohesive zone, the distribution of tractions ahead of the crack tip is not represented accurately. Therefore, a minimum number of elements,  $N_e$ , is needed in the cohesive zone to get successful FEM results. Several researchers attempted to define the minimum number of element, nevertheless, not a concrete outcome is given. For instance, Moes and Belytschko [206] suggested using more than 10 elements, while Falk et al. [207] used between 2 and 5 in their simulations or Davila and Camanho [208] proposed 3 elements in the cohesive zone for a Mode I delamination. Hence, based on the previous studies and accounting the computational demands as well, the author opted to use 4 elements to model the cohesive zone.

In terms of damage initiation criteria, the quadratic stress criterion, shown in Eq. 6.16 is used in this work. However, it is assumed that  $T_{II}$  equals  $T_{III}$ . Both Mode II and Mode III are matrix dominated interlaminar shear failures in two orthogonal directions, where tangential separation of the blankets occurs. Since matrix is treated in this study as a transversely isotropic material,



the interlaminar shear strengths  $T_{II}$  and  $T_{III}$  are assumed to be the same.

$$\left(\frac{\sigma_n}{T_I}\right)^2 + \left(\frac{\tau_s}{T_{II}}\right)^2 + \left(\frac{\tau_t}{T_{III}}\right)^2 = 1 \quad (6.16)$$

In the analysis presented herein, the quadratic power law, as expressed by Eq. 6.17, is used for energy-based propagation [202]:

$$\left(\frac{G_I}{G_{IC}}\right)^\kappa + \left(\frac{G_{II}}{G_{IIC}}\right)^\kappa + \left(\frac{G_{III}}{G_{IIIC}}\right)^\kappa = 1 \quad (6.17)$$

where,  $\kappa$  is the power index between 1 and 2. The power law has been selected instead of the Benzeggagh and Kenane [209] because the latter requires additional testing, namely Mixed Mode Bending, to further determine the coefficients required. A value of 1.5 was utilised as a compromise using the power law and for the analyses carried out in this work was considered acceptable. Lastly, the fibre bridging effect during interlaminar failure due to peel and shear was not accounted since the cohesive law of Simulia/Abaqus cannot be broken into more than one component, hence matrix cracking cannot be distinct from fibre bridging.

### 6.3.1 Mode I (DCB) FE validation

The described methodology and the selection of the parameters for the constitutive equations are investigated by analysing the Mode I - DCB and Mode II - 4pt-ENF tests with FE methods in Abaqus/Explicit. The dimensions of the DCB specimen are obtained from the actual tested specimens. It is 149 mm long, 20.5 mm wide with two 1.675 mm thick arms and it has an initial crack length of 59.3 mm as shown in Fig. 6.13. The lay-up configuration is the same as in the experimental study, namely  $[90^\circ/0^\circ]_{7S}$  and the material properties are shown in Tab. B.6 in Appendix B.1.3.

#### 6.3.1.1 Boundary Conditions

The FE model is consisted of the two sub-laminates which, until the initial crack tip, interact with each other only with a "hard contact" to eliminate penetration. After the crack tip,

supplementary to the "hard contact" property, a cohesive behaviour formulation is introduced with the implementation of the cohesive surfaces method. The loading pins, are modelled as "Reference points" (Points A and B) which are situated in the centroids of the pins and coupled through a rigid multi-body constrain to the projected area of the end-blocks. Translational constraints are applied on Point A in x and y-directions ( $U_X = U_Y = 0$ ), whereas in z-direction a tensile displacement rate of 5 mm/s is applied. In terms of rotational constraints, Point A is free to rotate around y-axis ( $UR_Y \neq 0$ ), while rotation around the other two axes is constrained ( $UR_X = UR_Z = 0$ ). On the other hand, in Point B, whilst the translational degrees of freedom are fully constrained ( $U_X = U_Y = U_Z = 0$ ), the rotational constraints are the same with Point A.

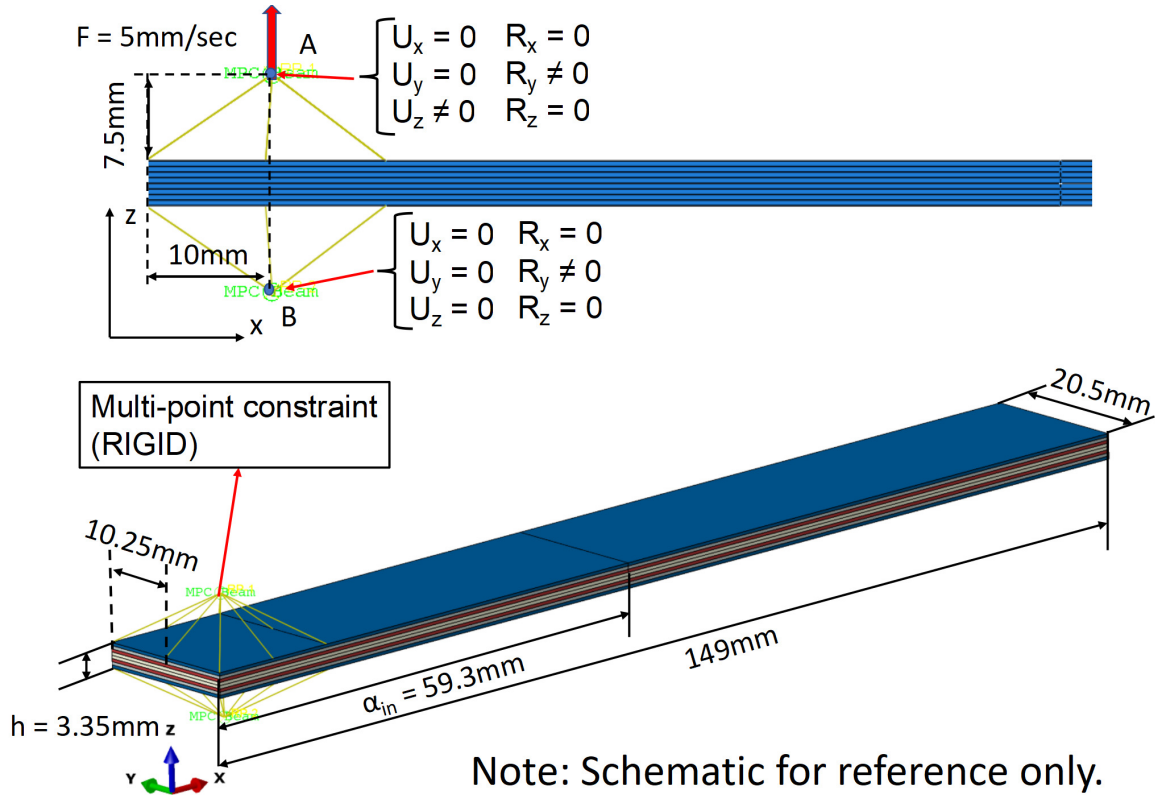


Figure 6.13: Illustration of boundary conditions and interface properties for the DCB FE model.

### 6.3.1.2 Analysis Parameters

The DCB specimen is composed by 8 continuum shell elements with reduced integration (SC8R) through the thickness corresponding to 4 elements per sub-laminate. Each element contains 2 NCF blankets, while each layer of the blanket is modelled with 3 integration points.

Several sets of mesh densities were simulated ranging from 3 mm to 0.5 mm to predict the peak load of DCB specimen (Fig. 6.14) and find the corresponding value of traction ( $T_I$ ). The penalty stiffness is obtained from Eq. 6.12 for discrete values of  $\alpha'$  from 40 to 80 after an iterative procedure so that the loading response of the numerical model matches the experimental curve.

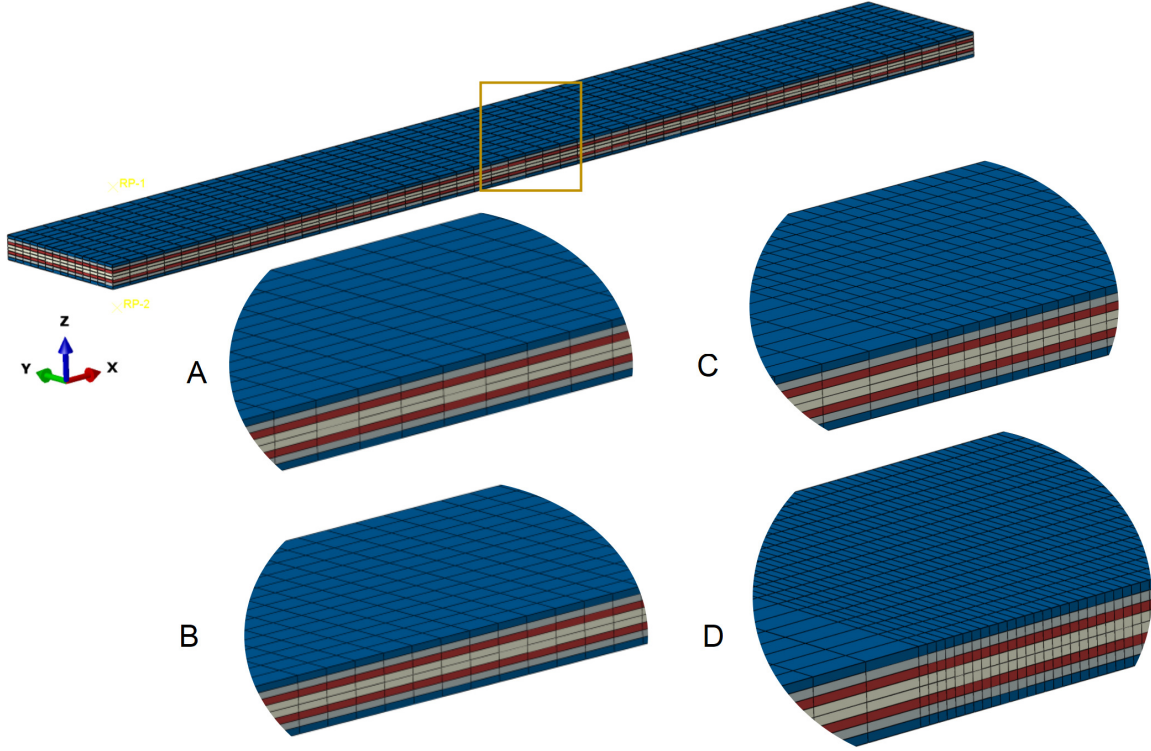


Figure 6.14: *Mesh sensitivity illustration of DCB specimen for element sizes of A:3 mm, B:2 mm, C:1 mm and D:0.5 mm.*

To avoid, however, using fine mesh to the simulation which would be computationally expensive, a strategy proposed by Bažan and Planas [210] was followed using lower interfacial strengths, whilst keeping the fracture toughness constant. This approach results in longer cohesive zones and coarser mesh but it can also improve the convergence rate of the solution [211]. The drawback of this approach is that the stress concentrations in the bulk material near the crack tip are less accurate when using a reduced interfacial strength value [204], but the mechanics of the dissipated energy is captured well. Hence, the interfacial normal and shear strengths that are required are derived from Eqs. 6.14 and 6.15 and receive the form:

$$T_{I/II} = \sqrt{\frac{9\pi E_{22} \cdot G_{I/IIc}}{32 \cdot N_e \cdot l_e}} \quad (6.18)$$

The number of elements,  $N_e$  in the cohesive zone is selected to be four as advised by Harper and Hallett [212]. Whilst the assigned global mesh of the specimen was 3 mm, mesh refinement was applied from the crack tip towards the back-end of the specimen in a single biased formulation setting as minimum element size 0.5 mm. In order to obtain accurate FEM results using CZM, the tractions in the cohesive zone must be represented properly by the finite element spatial discretization, hence the values of  $l_e$  are equal to 3 mm, 2 mm, 1 mm and 0.5 mm. To limit high frequency oscillations and maintain accurate results the viscosity coefficient was set to 0.001.

### 6.3.1.3 FE Results

The load-displacement response obtained for the different mesh densities is presented in Fig. 6.15 and compared against the average experimentally measured curve as generated from the MCC method in Chapter 4 (Fig. 4.31). This response indicates that converged solutions can be achieved when the interface strength is reduced according to Eq. 6.18. For element size of 1 mm and 0.5 mm more accurate results are obtained. For the larger element sizes, namely, 3 mm and 2 mm the peak load is overestimated by approximately 20 and 19% respectively. This is due to the single biased mesh refinement approach that has been implemented which forms elements of smaller sizes near the crack tip and required higher load for decohesion. Nevertheless the load response after failure initiation (app. 15 mm) correlates well with the experimental behaviour for all mesh sizes. With regards to the penalty stiffness parameter,  $\alpha'$ , the ideal value to match the loading curve of the tested specimen was found to be 75, corresponding to a  $K_n = 3 \cdot 10^5$  N/mm<sup>3</sup>, without identifying strong mesh dependency.

Moreover, the oscillations of the curves observed in Fig. 6.14 are mainly because of the cohesive surface approach which is characterised by an unstable response. This behaviour is intensified for the coarser mesh configurations and particularly for the 3 mm. In Tab. 6.2 the interlaminar properties of the DCB analyses as well as the peak load are summarised. As shown in Eq. 6.18, traction is also dependent on the interlaminar fracture toughness, therefore the traction values are calculated both for the interlaminar fracture of initiation ( $G_{IC_{Init.}}^{0^\circ}$ ) and the steady-state condition ( $G_{IC_{Prop.}}^{0^\circ}$ ). The latter is necessary for the numerical analyses of CT and LVI specimens which are characterised by long crack lengths.

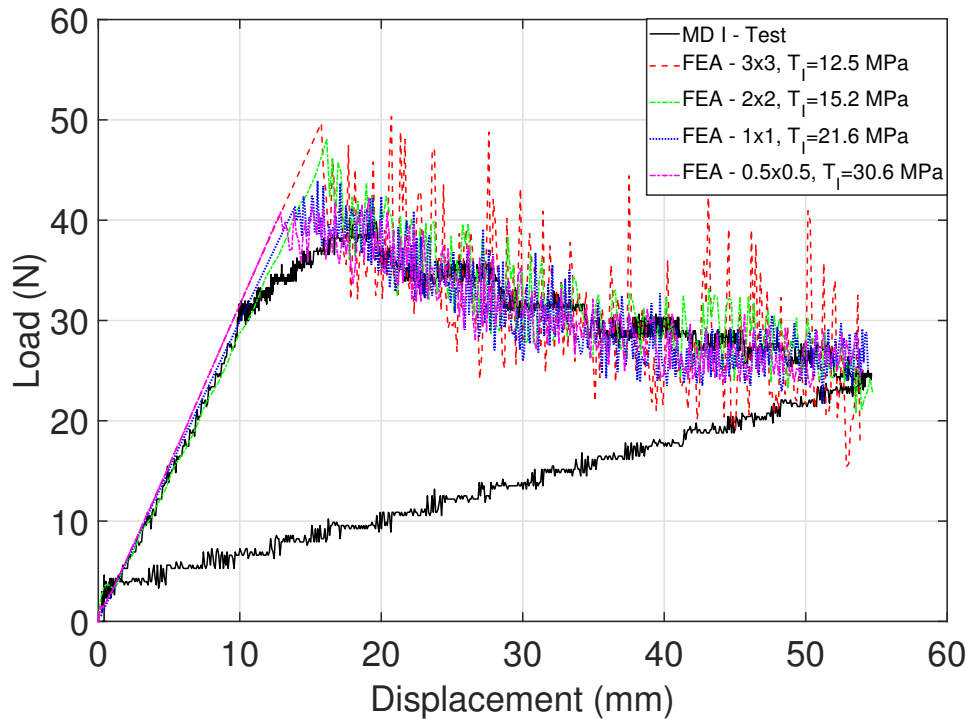


Figure 6.15: Predicted Load - Displacement response of DCB FE models of different mesh sizes and traction values for  $G_{IC_{Init.}} = 0.32 \text{ N/mm}$ .

| Mesh Size (mm) | # Elements | $T_I$ (MPa)                     |                                | Peak Load (N) |
|----------------|------------|---------------------------------|--------------------------------|---------------|
|                |            | $G_{IC_{Init.}} = 0.32$<br>N/mm | $G_{IC_{Prop.}} = 0.7$<br>N/mm |               |
| 3 x 3          | 5600       | 12.5                            | 18.45                          | 49.68         |
| 2 x 2          | 7056       | 15.2                            | 22.6                           | 48            |
| 1 x 1          | 11648      | 21.6                            | 32                             | 41            |
| 0.5 x 0.5      | 23552      | 30.6                            | 45.2                           | 40.8          |

Table 6.2: Summarised interlaminar properties of DCB simulation for different mesh densities.

Also, Fig. 6.16 illustrates the decohesion process for the variant mesh densities. The increased cohesive zone at initial stage of decohesion is apparent, as expected, for the coarser mesh configurations namely, 3 mm and 2 mm, whereas the finer meshes demonstrate much shorter cohesive zones.

### 6.3.2 Mode II (4pt-ENF) FE validation

The same modelling approach is used for the 4pt-ENF simulation. The 4pt-ENF specimen is 149 mm long, 22.8 mm wide with two 1.625 mm thick arms and the initial crack length is 47.6mm as shown in Fig. 6.17. The lay-up configuration is  $[90^\circ/0^\circ]_{7S}$  and the material properties are the

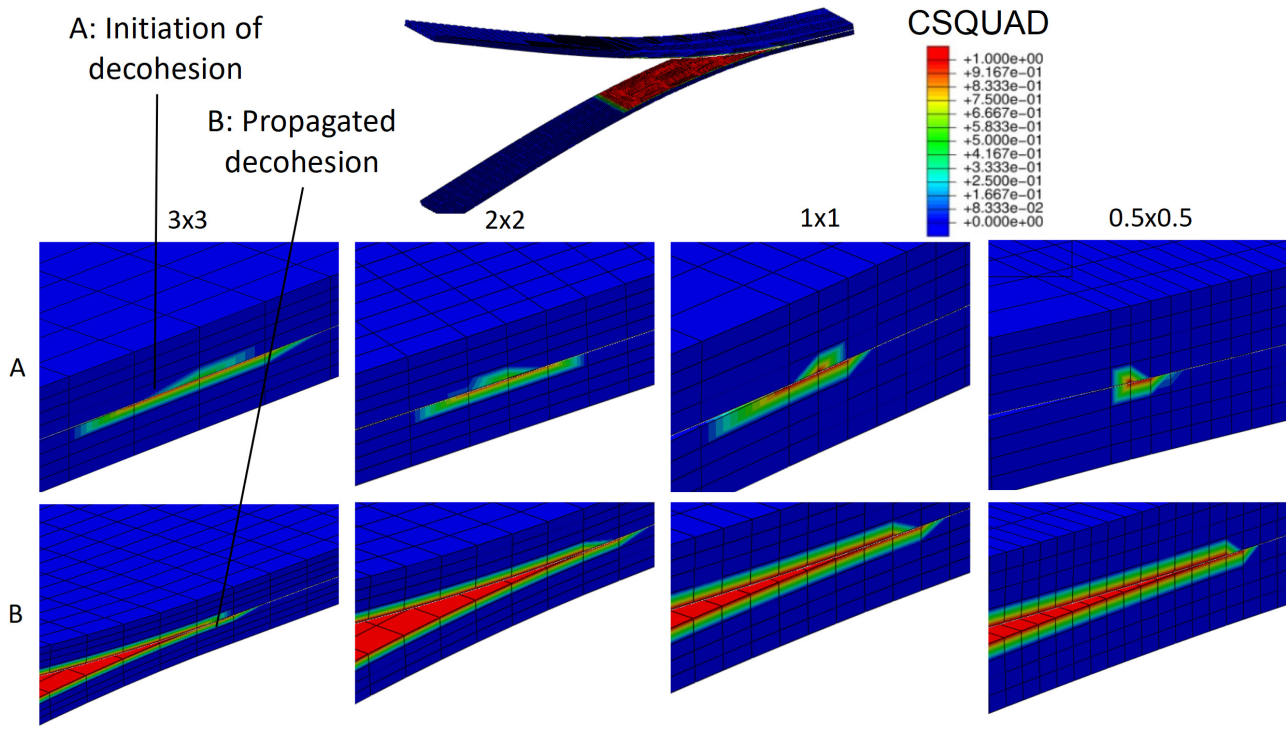


Figure 6.16: *Illustration of the DCB specimen decohesion at (A) the initial stage and (B) displacement equal to 40mm for the different element sizes.*

same as for the DCB case. The positioning and the size of the testing fixture, i.e., the relative distance of the specimen to the loading and support rollers, is compliant with the experimental study. Both loading and support pins are modelled as 50 mm long "discrete rigid" shell parts of 6 mm and 10 mm diameter respectively.

### 6.3.2.1 Boundary Conditions

Fig. 6.17 illustrated the boundary conditions and the interactions of the 4pt-ENF FE model. The pivoting point of the test fixture is represented in the FE model by "Reference point" A, which is constrained to the x- and y-directions ( $U_X = U_Y = 0$ ), whereas in z-direction a compressive displacement rate of 5 mm/s is applied. Point A is free to rotate around y-axis point, while the other two rotations are constrained ( $U_X = U_Y = U_Z = 0$ ). A rigid multi-point constraint is applied between point A and the outer shell surface of the loading pins. The inertia of each pin is assigned to the centroid as concentrated mass equal to 100 g and 200 g for the loading and support pins respectively as measured by the author during the experimental procedure. As for the loading pins, the rigid multi-point constraint is applied

between the centroid and the shell surface of the support pins which are fully constrained in terms of translation and rotation.

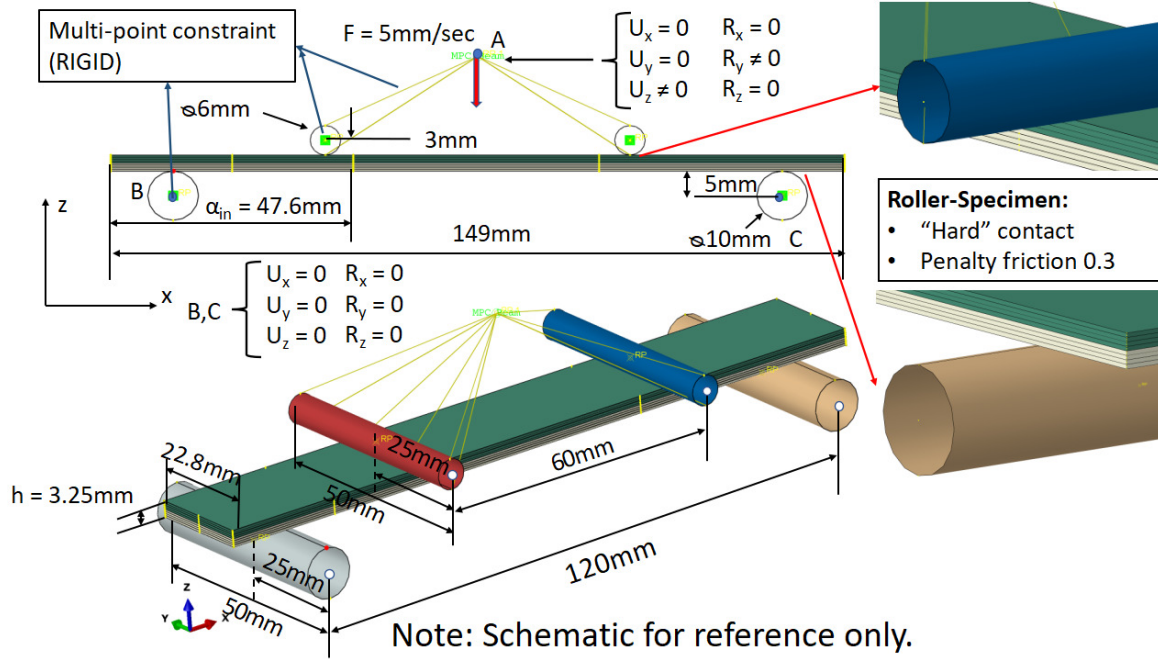


Figure 6.17: Illustration of boundary conditions and interface properties for the 4pt-ENF FE model.

The specimen is modelled in the same manner as in the DCB case. The pin and support rollers interact with the specimen through a "hard contact" normal property, whereas the tangential behaviour is modelled with a penalty friction equal to 0.3 as suggested by Sung and Suh [213] for friction coefficient of CFRP against steel.

### 6.3.2.2 Analysis Parameters

The parameters of the analysis for the 4pt-ENF follow the same principle as for the DCB case. The only difference regards, the estimation of the stiffness penalty,  $K_s$ , which is obtained from Eq. 6.13 and is found to be equal to  $2 \cdot 10^5 \text{ N/mm}^3$ . The same mesh sensitivity study is performed for this case too (Fig. 6.18).

### 6.3.2.3 FE Results

The load-displacement response obtained for the different mesh densities is presented in Fig. 6.19 and compared against the average experimentally measured curve as generated from the



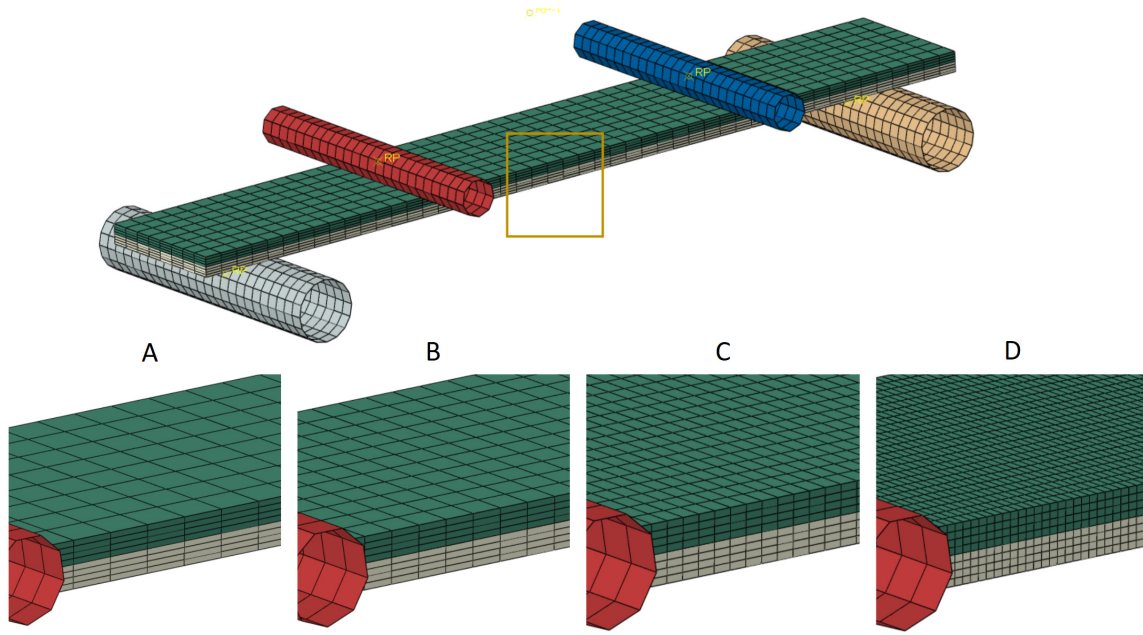


Figure 6.18: *Mesh sensitivity illustration of 4pt-ENF specimen for element sizes of A: 3 mm, B: 2 mm, C: 1 mm and D: 0.5 mm.*

CC method. In principle the numerical results show close agreement with the test curve and predict with accuracy the peak load regardless the mesh size. However, when element length is increased to 2 mm and 3 mm, the models exhibit significant oscillations in the propagation regime. This suggests that the optimal size of the element for an accurate analysis lies between 0.5 mm and 1 mm.

As a conclusion, the varying interfacial strength for the 4pt-ENF specimen, can satisfactorily capture the peak load prior to crack propagation particularly for the finer meshes (2 and 1 mm), which implies that there is no significant loss of stiffness during the elastic loading regime. Though, with interfacial strength increased to a more realistic value of 45 MPa, a much closer match to the test response is obtained. The relative poorer results gained using lower values of interfacial strength are believed to be due to the very long numerical cohesive zone lengths and hence significant softening ahead of the crack tip.

As for the DCB, reduced interfacial strength increases the accuracy of the analysis in the propagation regime. However, the initial point of crack propagation for a fine mesh cannot be regained. This is likely to be due to the coarseness of the mesh combined with the very sudden nature of crack propagation in the 4pt-ENF specimen. Tab. 6.3 summarises the interlaminar



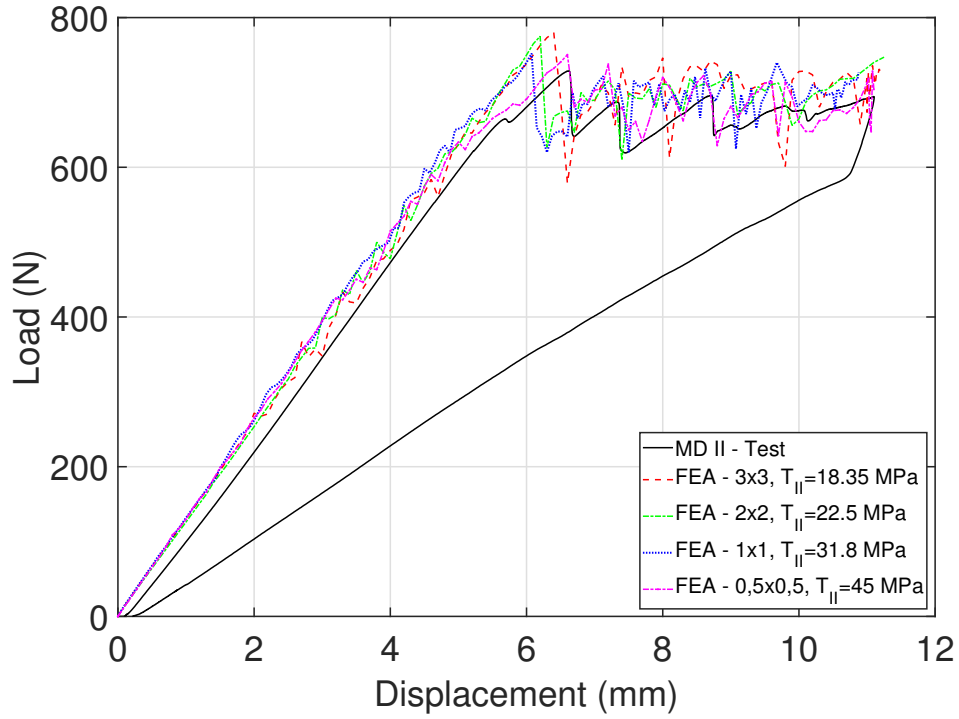


Figure 6.19: Predicted Load - Displacement response of 4pt-ENF FE models for different mesh sizes and traction values.

properties of the 4pt-ENF analyses, the peak loads and the traction values for the steady-state interlaminar fracture toughness  $G_{IIC_{Prop.}}$ .

| Mesh Size (mm) | # Elements | $T_{II}$ (MPa)                  |                                | Peak Load (N) |
|----------------|------------|---------------------------------|--------------------------------|---------------|
|                |            | $G_{IIC_{Init.}}=0.693$<br>N/mm | $G_{IIC_{Prop.}}=1.67$<br>N/mm |               |
| 3 x 3          | 8644       | 12.5                            | 18.35                          | 779.8         |
| 2 x 2          | 13824      | 22.5                            | 22.6                           | 775           |
| 1 x 1          | 29620      | 31.8                            | 49.3                           | 752           |
| 0.5 x 0.5      | 79780      | 45                              | 69.8                           | 750           |

Table 6.3: Summarised interlaminar properties of 4pt-ENF simulation for different mesh densities.

Unlike, the DCB case, load displacement curves do not exhibit many strong oscillations, mainly due to the nature of bending which is characterised by more stable crack propagation, nevertheless, some excessive load drops are observed particularly for the mesh densities 3 mm and 2 mm. Also, Fig. 6.20 illustrates the decohesion at the initial stage (A) and during evolution (B) for all mesh densities.

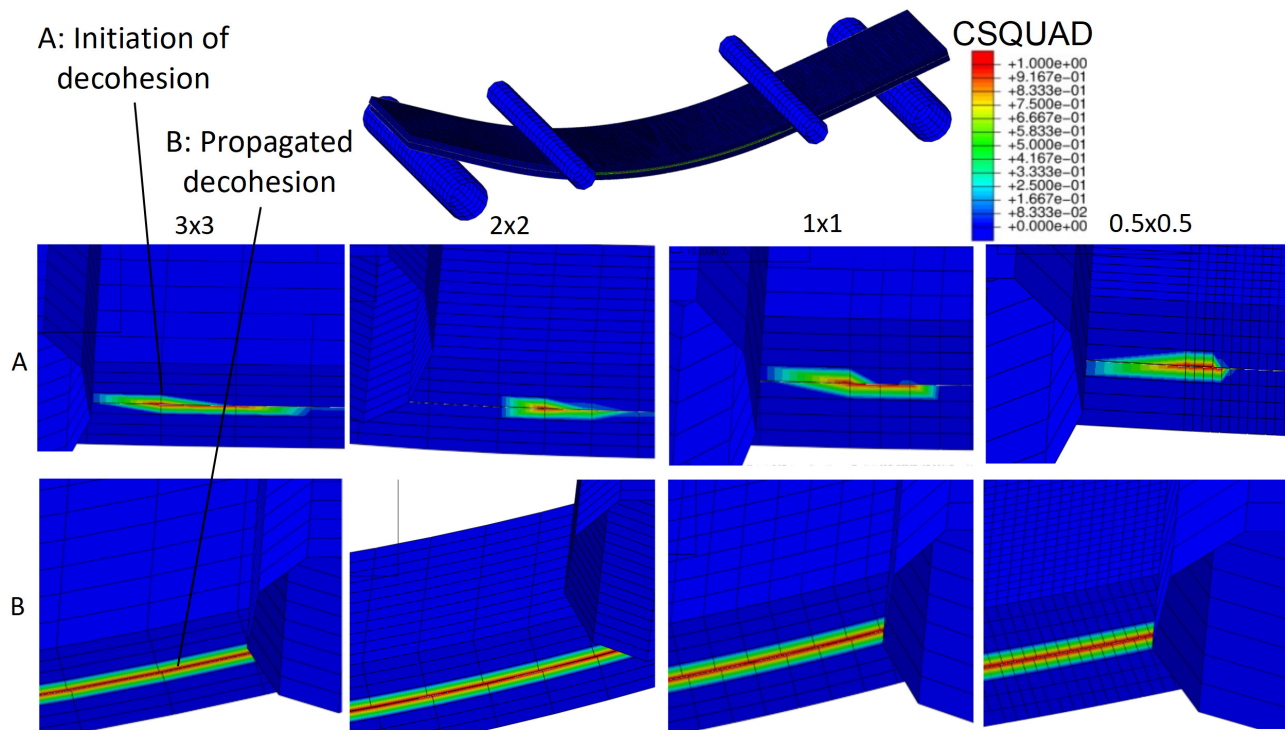


Figure 6.20: Illustration of the 4pt-ENF decohesion at (A) the initial stage and (B) displacement equal to 40mm for the different element sizes.

# Chapter 7

## Numerical Analysis of In-plane loading cases

### 7.1 Introduction

The prediction capabilities of the constitutive model presented in Chapter 5 to forecast damage onset, are evaluated in quasi-static in-plane conditions at coupon level. In particular the tensile, in-plane shear and compression load scenarios are numerically modelled and compared against the corresponding test data. Continuum shell elements with reduced integration (SC8R) were utilised to model the NCF blankets of the specimens, while each layer of the NCF blanket was modelled with 3 integration points through-the-thickness. The fibres within the layers were homogenised and assumed to be evenly distributed.

Moreover, fibre misalignment is not represented as a geometric feature, but instead, it is introduced at layer level directly to the material card based on the OM observations for the tensile, compression and in-plane shear specimens. In particular, the regions on the specimens in which similar fibre misalignment was observed, are grouped together to specific partitions of the FE model. There is no limitation regarding the number of partitions, as it solely depends to the heterogeneity of fibre misalignment within the specimen. An advantage of this approach is that

the fibre misalignment does not depend on the size of mesh since it is assigned to the geometric partitions and not to each element individually (as local coordinate system), a labour intensive procedure particularly in larger structural components. With regards to the in-plane fibre misalignment,  $\phi_I$ , the values assigned to the top and bottom layers of the specimen are obtained directly from the OM, whereas for the interior layers, the average value of  $\phi_I$  between the top and bottom layers was estimated. On the other hand, the out-of-plane fibre misalignment,  $\theta_I$  is assigned at each layer individually.

Due to the applied high loading rates (i.e. 3 mm/s) in the FE analyses, it was considered necessary to increase proportionally the values of Rayleigh damping techniques which are coupled to material density; however, the inertia forces have to be significantly smaller than the forces generated from the stiffness of the specimen [13], otherwise unreal modes are generated. Monitoring the kinetic energy/internal energy ratio of the system is evident whether the above condition is met and a ratio of the order of 0.1 is typically accepted. This principle is followed for all the FE analyses carried out in this project. Concerning the correlation between the numerical and test results, an up to 5% discrepancy was considered acceptable. It is noted that this level of deviation is justified in the view of the many sources that underlie it, ranging from mechanical testing to measurement equipment setup. Also, no artifice was used to initiate failure at any specific location or along any certain direction.

## 7.2 Tension loading

Due to the unstable failure of the specimens which was observed during testing, and to maintain the computational time within acceptable limits, only the gauge length of the tension specimen ( $[0^\circ/90^\circ]_{4S}$ ) is numerically modelled. The numerical model is compared against specimen RFI\_2-T\_5, hence the specimen dimensions are defined as presented in Tab. A.1 in Appendix A.2.

### 7.2.1 Boundary conditions

In terms of boundary conditions, one end of the specimen was constrained against translation longitudinally ( $U_x = 0$ ), while only the lower edge of that end was constrained transversely through-the-thickness of the specimen ( $U_z = 0$ ). The upper edge of that end was free for translation in the  $z$ -axis to account the Poisson's effect ( $xz$  plane). Translational constraints across the specimen ( $U_y = 0$ ) are applied to the vertical edges at the one side of the specimen. As previously, the opposite side was free for translation in the  $y$ -axis, so that the  $xy$ -plane Poisson's effect is accounted. The tensile load is expressed as a ramped displacement,  $F$  versus time boundary condition applied at the free end with ultimate value of 3 mm/sec (Fig. 7.1) depicts the described boundary conditions.

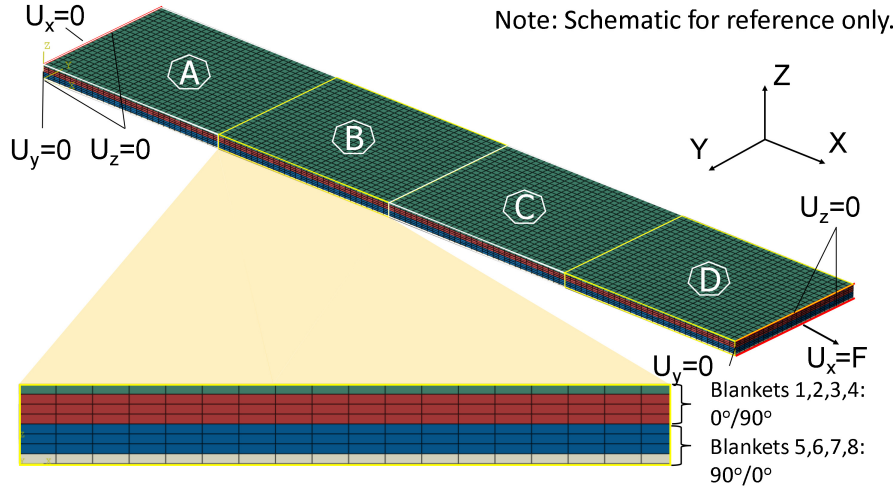


Figure 7.1: *Illustration of boundary conditions and partitioning of the tensile specimen.*

### 7.2.2 Analysis parameters

As illustrated in Fig. 7.1 the specimen is partitioned in four sections (A - D) through-the-thickness. Hence, a combination of four sections per layer is created where in each section of each layer a specific value of  $\phi_I$  and  $\theta_I$  is assigned based on the experimental recordings of fibre misalignment on the specimen as presented in Chapter 3. The values of  $\phi_I$  and  $\theta_I$  introduced in each section and layer for this simulation are presented in Tabs. B.8 and B.9 of Appendix B.2.1 for each section and layer of the specimen (Fig. 7.1). A sensitivity analysis is employed to validate the mesh objectivity of the damage model. Four different in-plane mesh densities

were examined of 8 mm, 4 mm, 2 mm and 1 mm, assigned with 1, 2, 4 and 8 elements through the thickness of the specimen respectively. In the case of the 8 mm x 8 mm element size model, each continuum shell element contains 16 layers where three integration points per layer were allocated. Similarly, the 4 mm x 4 mm element FE model contained 8 layers per element, while the 2 mm x 2 mm element FE model contained 4 layers, and lastly the 1 mm x 1 mm element contained 2 layers. The mesh properties are summarised in Table 7.1 and mesh illustrations are presented in Fig. 7.2.

| Simulations | Element size | # Elements | Layers/Element (IP) |
|-------------|--------------|------------|---------------------|
| T_8x8       | 8x8          | 48         | 16                  |
| T_4x4       | 4x4          | 372        | 8                   |
| T_2x2       | 2x2          | 3024       | 4                   |
| T_1x1       | 1x1          | 25200      | 2                   |

Table 7.1: Summarised mesh properties of the numerical models for tension.

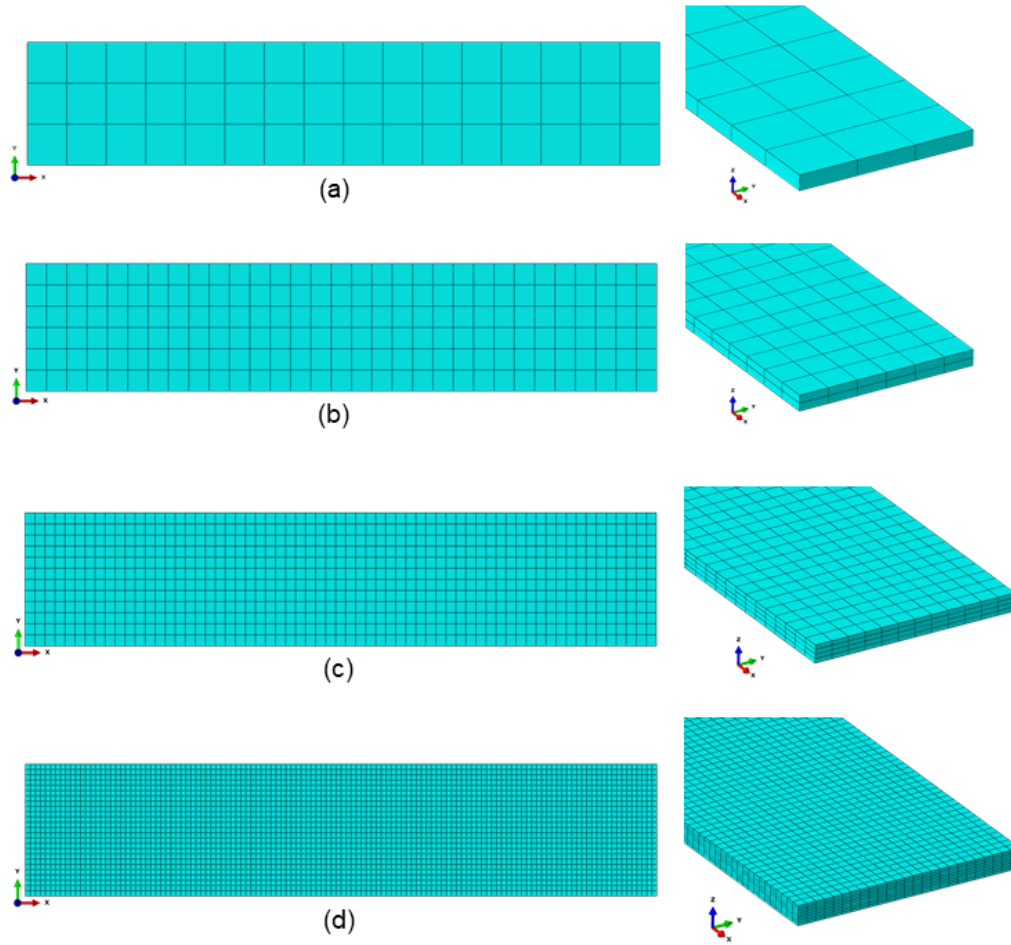


Figure 7.2: Illustration of the mesh sensitivity study for tension FE models, with element mesh densities of: a) 8 mm x 8 mm b) 4 mm x 4 mm c) 2 mm x 2 mm and d) 1 mm x 1 mm.

In this analysis, no interface properties were implemented between the NCF blankets since the influence of possible delamination was not considered to be essential. Also a second FE analysis with a 1 mm x 1 mm mesh density is performed, in which the average values for misalignment are used across all layers and sections namely,  $\phi_I = 2.1^\circ$ ,  $\theta_I = 3.4^\circ$  and compared against the detailed configurations.

### 7.2.3 Results

In Fig. 7.3 the numerical models are compared directly to the experimental testing through the stress-strain response, which reveals a mesh objective behaviour. The stress was calculated as the division of force over the cross-sectional area of the specimen, whereas the relative displacement is calculated as the extension between two points on specimen over the initial gauge length. The detailed configured FE models correlated very well with the experimental data, both in capturing the tensile stiffness and predicting the ultimate stress and strain with a less than 5% discrepancy. The numerical model with the average values of fibre misalignments, appears to overestimate the stiffness of the tested specimen by 6% which is almost double than the detailed configurations, but still within the range of acceptance. As expected though, the simplified FE model, captured accurately the tensile strength of the specimen, since the failure criteria in fibre and matrix tension are not affected by fibre misalignment.

The tensile failure stress and strain of the FE models are compared with the test results and presented in Tab. 7.2. Column "PD" corresponds to the percent deviation between the numerical and experimental results, and equals to:  $PD = \frac{FE_{Value} - Test}{FE_{Value}}$ .

|            | <b>X<sub>T</sub></b> (MPa) | <b>PD</b> (%) | <b>ε<sub>fail</sub></b> (-) | <b>PD</b> (%) |
|------------|----------------------------|---------------|-----------------------------|---------------|
| T_5 (Test) | 1252.7                     | -             | 0.0191                      | -             |
| T_8x8      | 1300                       | 3.7%          | 0.01937                     | 1.4%          |
| T_4x4      | 1305                       | 4.2%          | 0.0194                      | 1.5%          |
| T_2x2      | 1301                       | 3.8%          | 0.01938                     | 1.4%          |
| T_1x1      | 1305                       | 4.2%          | 0.01938                     | 1.4%          |
| T_1x1-Avg  | 1310                       | 4.5%          | 0.0188                      | 1.6%          |

Table 7.2: Summarised FE analysis results of  $[0^\circ/90^\circ]_{4S}$  tension compared with test results.

In Figs. 7.4 and 7.5, the failure indices are presented for the different mesh densities and

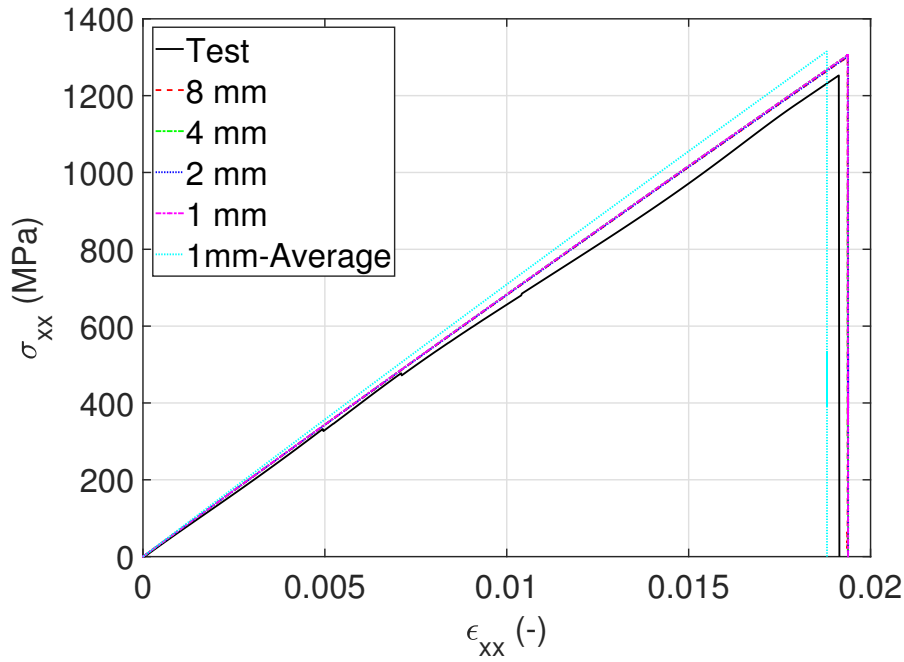


Figure 7.3: *Stress-strain relationship for tensile specimen simulations of discrete element sizes compared with test results.*

correspond to the fracture band of the  $0^\circ$  and  $90^\circ$  layers at the midsection of the specimen respectively. The elements failed close to the load introduction points, probably because the end-tabs of the specimen are not included to the numerical modelling. An important parameter that could be associated with the location of fracture is that the gripping force on the end-tabs is replaced by a concentrated displacement. Nevertheless, the failure morphology correlates well with the fractured areas of the actually tested specimen as it is illustrated in Fig. 7.5. The fracture band is perpendicular to the loading direction and extended along the width of the specimen as exactly exhibited in the actual failed specimens.

### 7.3 In-plane shear loading

The prediction of the shear response either at specimen or component level is highly important for evaluating the prediction capabilities of a constitutive failure model, therefore the in-plane shear test is numerically modelled too. Due to the stable crack propagation during the in-plane shear test, which occasionally is initiated from the end-tab region, it was decided to numerically model a full specimen, including the end-tabs, with the lay-up  $[45^\circ/-45^\circ]_{4s}$ . The in-plane shear



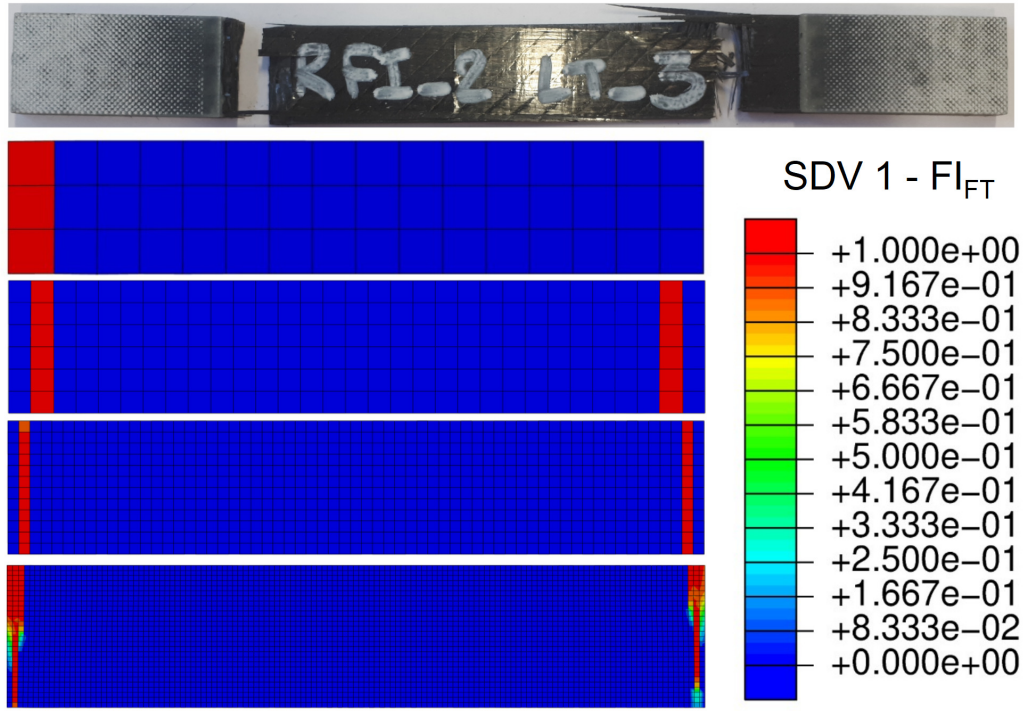


Figure 7.4: Failed specimen in tension and tensile failure of the  $0^\circ$  layer at  $\epsilon_{xx} = 0.019382$ .

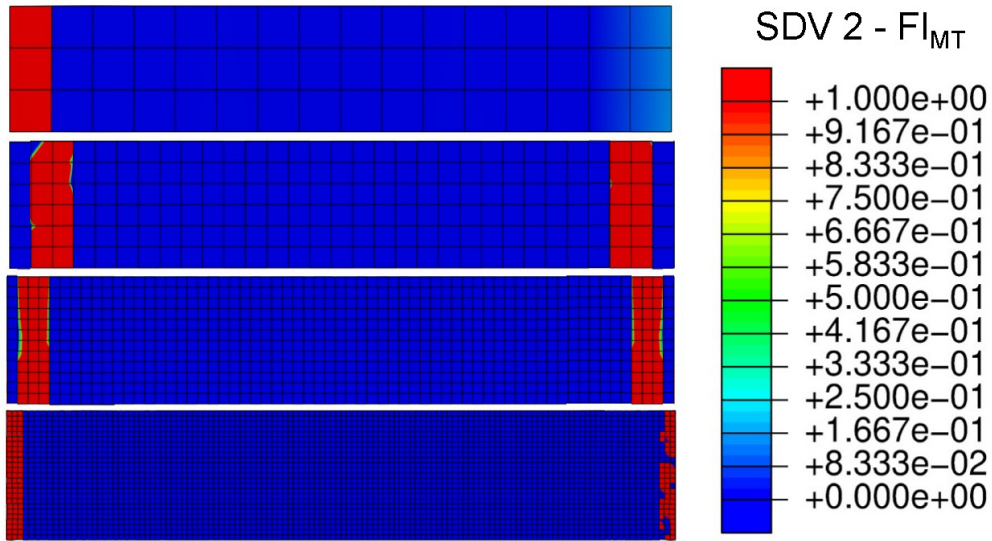


Figure 7.5: Matrix tension failure of the  $90^\circ$  layer at  $\epsilon_{yy} = 0.01494$ .

numerical model is compared with specimen IPS-1, hence the specimen dimensions are defined as presented in Tab. A.2 of Appendix A.2. The constitutive failure model is applied at layer-level (each NCF blanket is consisted of two layers), where at each layer three integration points are allocated.

### 7.3.1 Boundary conditions

The boundary conditions (Fig. 7.6) differ from the tension FE model because of the end-tabs modelling. Longitudinal (x - axis) translational constraint ( $U_x = 0$ ) is applied at the end-tabs opposite to the tensile ramped displacement,  $F$  (3 mm/s), which is transmitted by shear via the end-tabs. The gripping force on the end-tabs, generated by the hydraulic grips, is commonly assumed to be 10 - 15% larger than the maximum axial test load to be applied [214], which for the in-plane shear tests of this study corresponds to 12 kN. Hence the gripping force is estimated to be 13.8 kN per end-tab, resulting in a pressure load,  $P$  of 11 N/mm<sup>2</sup> based on the area of the end-tabs (25 mm x 50 mm). The translational constraints in the z-direction represent the reaction at the opposite grip of the fixture generated by the applied pressure. Translational constraints across the specimen ( $U_y = 0$ ) are applied to both faces of the end tabs.

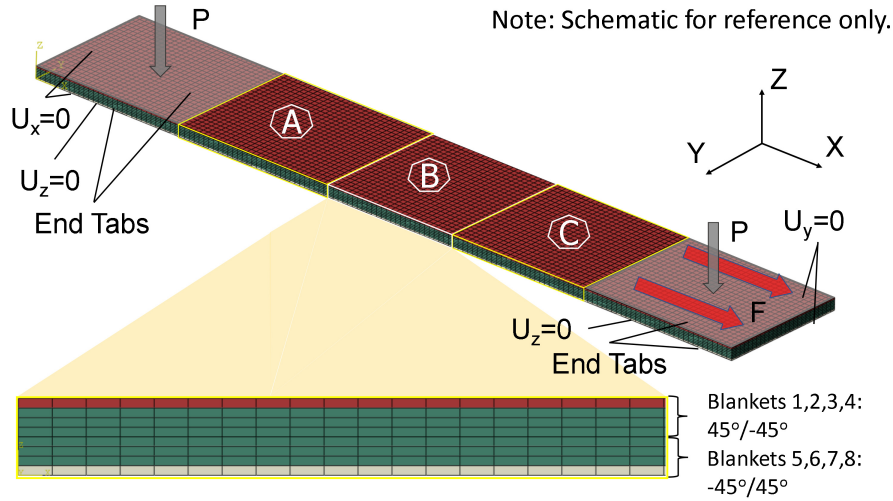


Figure 7.6: *Illustration of boundary conditions and partitioning of the in-plane shear specimen.*

### 7.3.2 Analysis parameters

A mesh objectivity analysis is employed for the shear FE model too, applying the same in-plane mesh densities as for tension FE model and through-the-thickness discretisation (Tab. 7.3). The values of  $\phi_I$  and  $\theta_I$  for each section and layer of the specimen (Fig. 7.6) are presented in Tabs. B.10 and B.11 of Appendix B.2.2, whereas the material properties at layer level are summarised in Tab. B.6 of Appendix B.1.4. Like in tension, a second FE analysis with a 1 mm x 1 mm mesh density is performed, in which the average values for misalignment are used

across all layers and sections namely,  $\phi_I = 1.8^\circ$ ,  $\theta_I = 3.4^\circ$  and compared against the detailed configurations.

| Simulations | Element size | # Elements | Layers/Element (IP) |
|-------------|--------------|------------|---------------------|
| IPS_8x8     | 8x8          | 96         | 16                  |
| IPS_4x4     | 4x4          | 744        | 8                   |
| IPS_2x2     | 2x2          | 5952       | 4                   |
| IPS_1x1     | 1x1          | 49800      | 2                   |

Table 7.3: *Summarised mesh properties of the numerical models for in-plane shear.*

With regards to the interface modelling, as stated in Chapter 6, the numerical model utilises a bilinear approach, requiring a single fracture toughness value. Since the gauge length of the specimen is essentially longer than 10 mm, which is the critical crack opening after which steady state delamination is achieved as presented in the DCB and 4pt-ENF tests in Chapter 4, the steady state values for interlaminar fracture toughness ( $G_{IC_{Prop.}}$  and  $G_{IIC_{Prop.}}$ ) are used. If  $G_{IC_{Init.}}$  and  $G_{IIC_{Init.}}$  are introduced instead, then premature delamination is expected to occur, which is not representative to the actual failure sequence.

### 7.3.3 Results

The main interest of the in-plane test is the examination of the shear response, since it is not trivial to achieve an accurate match between the computational and experimental stress-strain response. The shear stress was calculated as the division of force over the cross-sectional area of the specimen, whereas the relative displacement is calculated as the extension between two points on specimen over the initial gauge length. In Fig. 7.7, the shear stress-strain curves are shown for the different mesh densities compared with the test results. The numerical models predicted the experimental behaviour well. Somewhat higher stresses appear in the non-linear transition for the FE models meaning that the uniformly absorbed energy over all the specimen in that region is higher than in shear test. The stress-strain response of the FE model with the averaged values of misalignment is deliberately offset by 0.005 to avoid confusion with the other curves. It is apparent that the shear behaviour of that model is identical to the detailed configuration. Neither the shear modulus nor the shear failure parameters (stress and strain)

changed. That was expected since in-plane shear is governed by a matrix dominated failure which is not affected by fibre waviness.

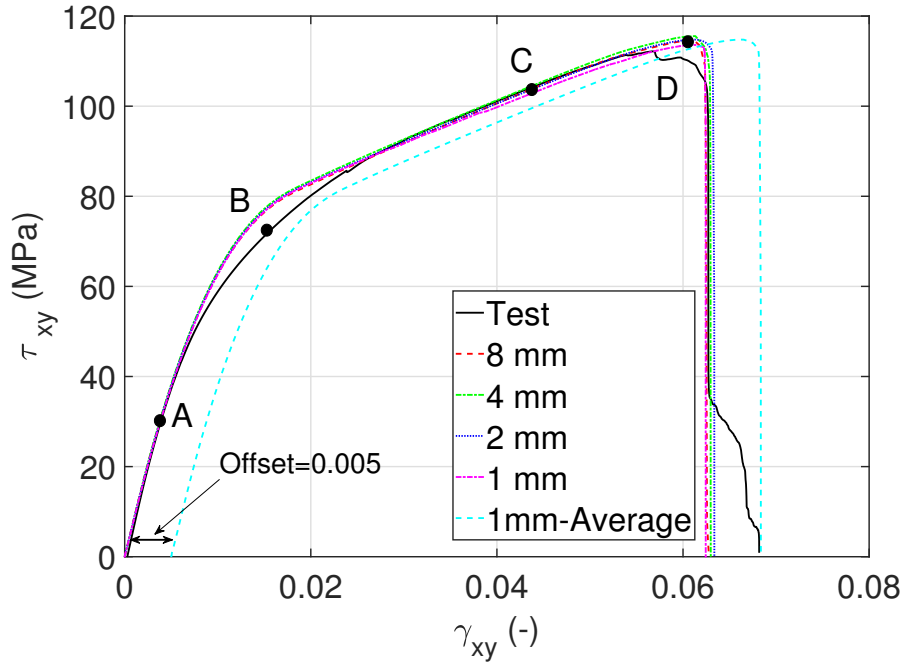


Figure 7.7: *Stress-strain response for in-plane shear specimen simulations of the discrete element sizes compared with test results.*

In terms of the crack prediction and failure morphology of the FE model, in Fig. 7.8 is noticed that only the FE case with 8 mm x 8 mm element size, did not capture successfully the crack morphology, although the respective stress-strain plot correlated very well with the experimental data. Concerning the simulations of the other three element sizes, shear failure is predicted as a localised crack and appears not to be affected by the size of the element, while they agree well with the failure pattern of the failed specimen.

A comparison between the shear strain maps of FEA and DIC at failure is illustrated in Fig. 7.9, which reveals that the FEA contours for the 4, 2 and 1 mm element sizes showed good agreement with the DIC observations and predicted realistically the failure pattern of the specimen. On the other hand, the 8 mm element configuration predicted the location of the failure but not the morphology of the localised strain.

The in-plane shear strain field,  $\gamma_{xy}$  from the DIC camera is compared with the specimens of the different mesh densities at four discrete points (A, B, C and D) are shown in Figs. 7.10, 7.11,

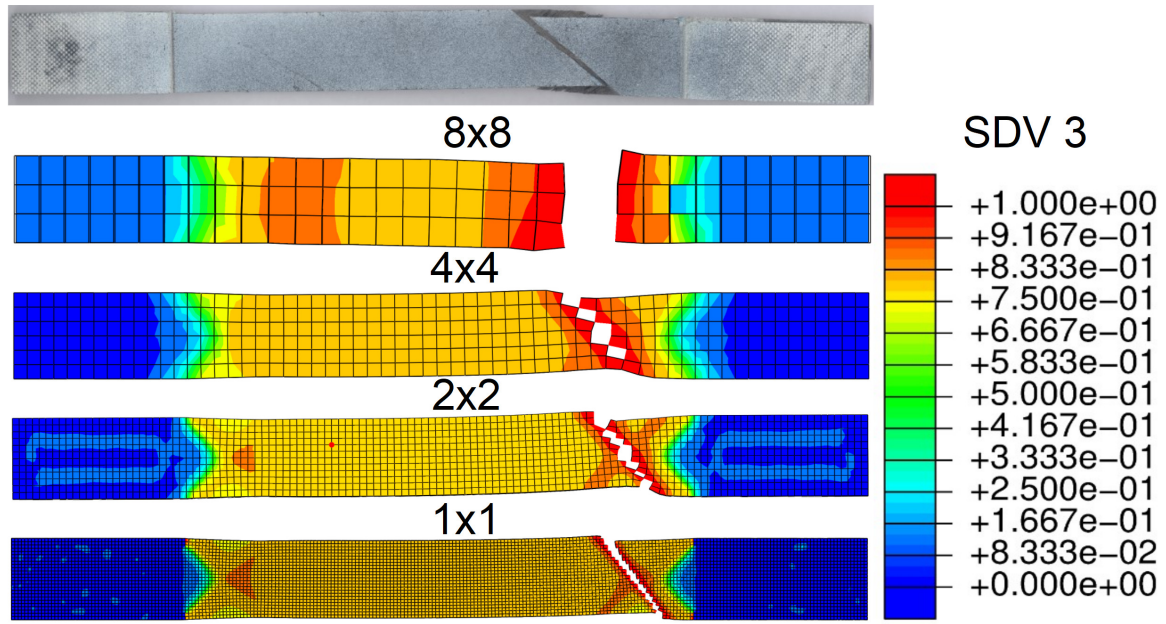


Figure 7.8: Crack morphology prediction by the FE models of four mesh densities under in-plane shear and comparison against a tested specimen.

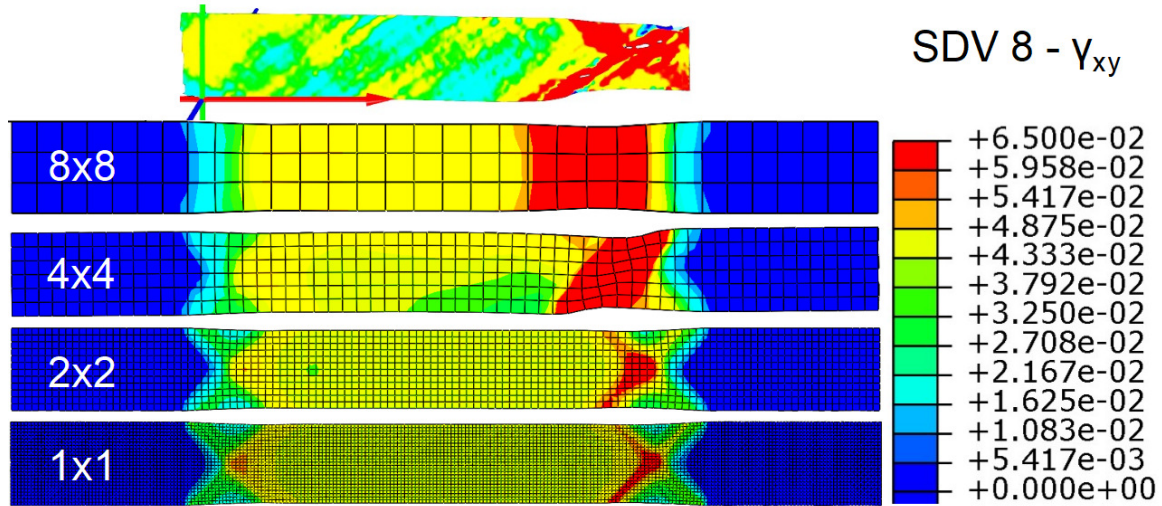


Figure 7.9: Ultimate shear strain fields ( $\gamma_{xy}$ ) compared with DIC surface contour.

7.12 and 7.13. In particular, point A corresponds to the end of the linear elastic region, point B to the linear - non-linear transition behaviour, point C to the final linear modulus region and point D to the failure of the specimen.

In the in-plane shear test, a shear band is formed where damage propagates rapidly and leads to failure, (see point D in Fig. 7.8). Formation of the shear bands initiates as seen in Fig. 7.13 for all four mesh configurations, where the red colours indicate the localised highly damaged areas of the FE models and the respective shear strain field as obtained from the DIC camera.



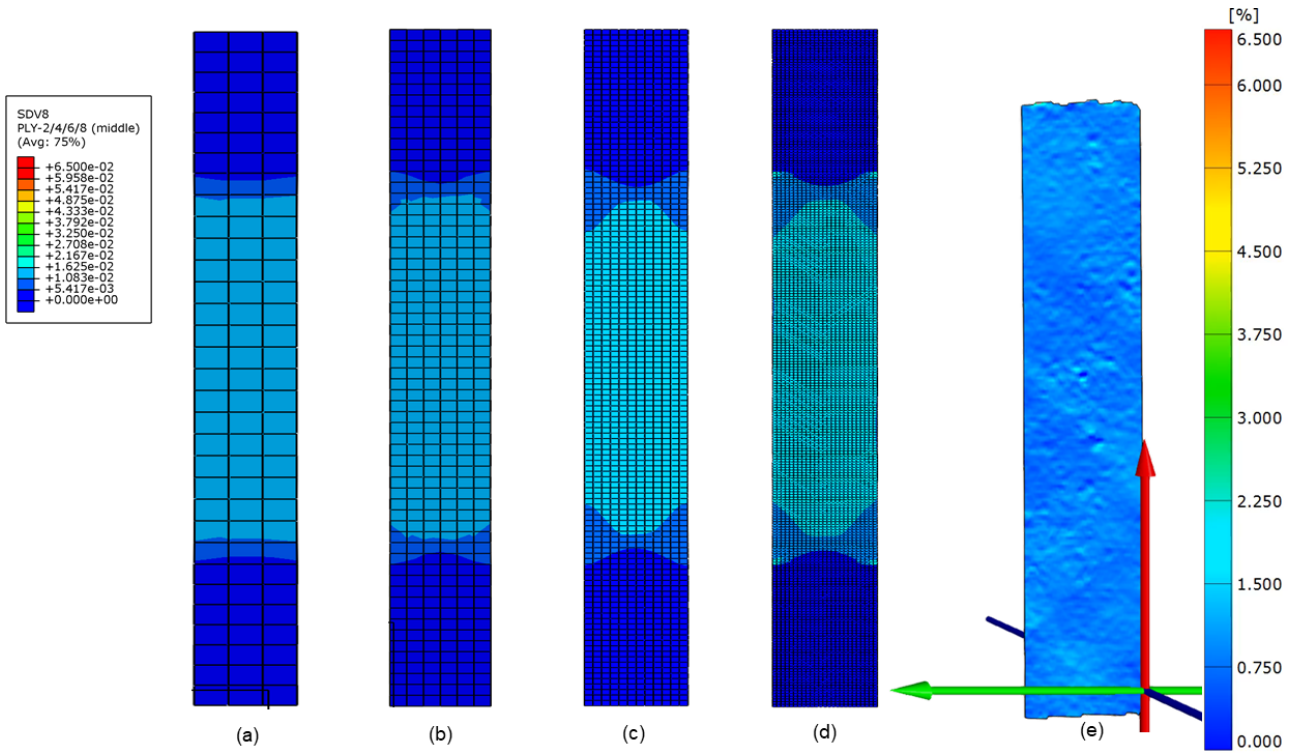


Figure 7.10: Illustration of shear strain fields ( $\gamma_{xy}$ ) at point A for: a) 8 mm x 8 mm, b) 4 mm x 4 mm, c) 2 mm x 2 mm, d) 1 mm x 1 mm mesh densities and e) DIC surface contour.

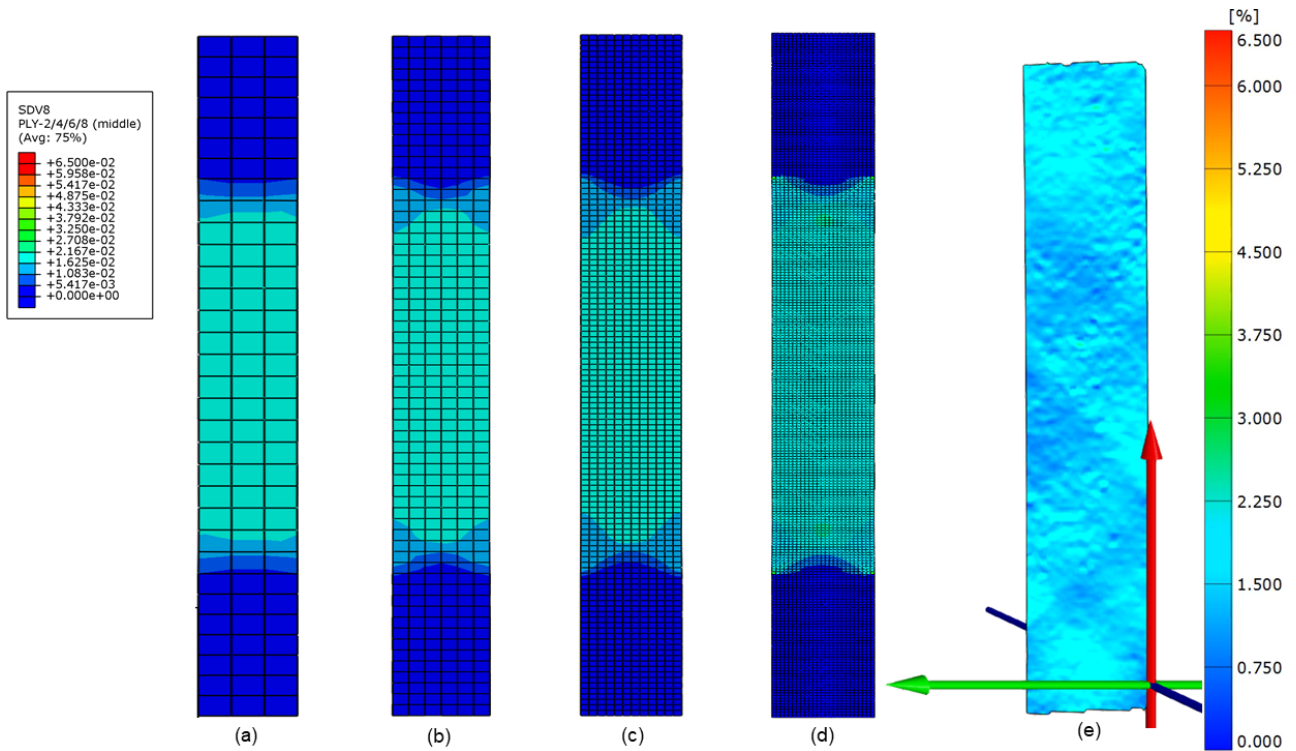


Figure 7.11: Illustration of shear strain fields ( $\gamma_{xy}$ ) at point B for: a) 8 mm x 8 mm, b) 4 mm x 4 mm, c) 2 mm x 2 mm, d) 1 mm x 1 mm mesh densities and e) DIC surface contour.

The failure pattern particularly for the 2 mm x 2 mm and 1 mm x 1 mm FE models is in good agreement with the DIC observations.

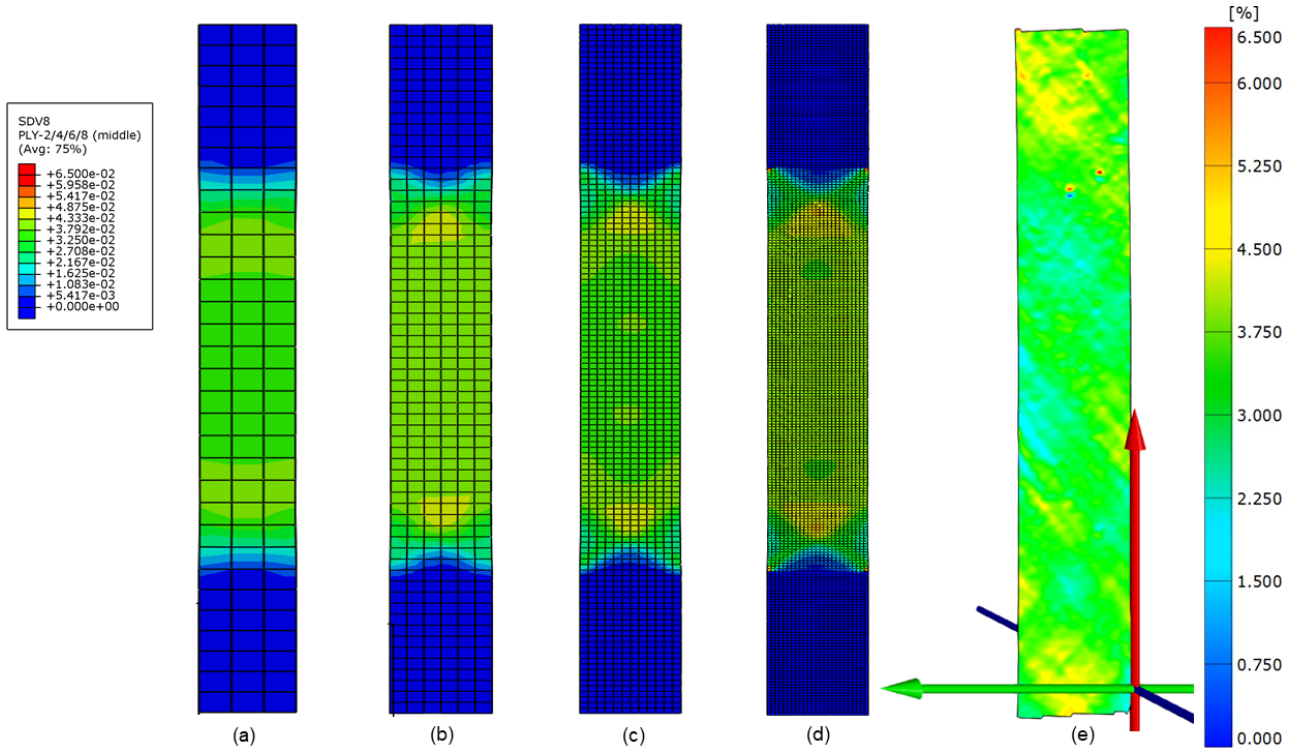


Figure 7.12: Illustration of shear strain fields ( $\gamma_{xy}$ ) at point C for: a) 8 mm x 8 mm, b) 4 mm x 4 mm, c) 2 mm x 2 mm, d) 1 mm x 1 mm mesh densities and e) DIC surface contour.

The evolution of the shear damage variable at four discrete points of the stress-strain curve is shown in Fig. 7.14. The successful correlation between the numerical and experimental results is obtained mainly due to the scissoring effect that was taken into account in the non-linear shear analytical formulation.

With regards to the prediction of delamination, in Fig. 7.15, the interface failure at the midplane of the FEA model (1 mm x 1 mm element size) for time step just prior to failure is illustrated and reveals that the delaminated areas are predicted to be close to the end-tabs and around the crack. The numerical model is compared with the actual failed specimen, and it is shown that there is a good phenomenological correlation with regards to the delaminated regions.

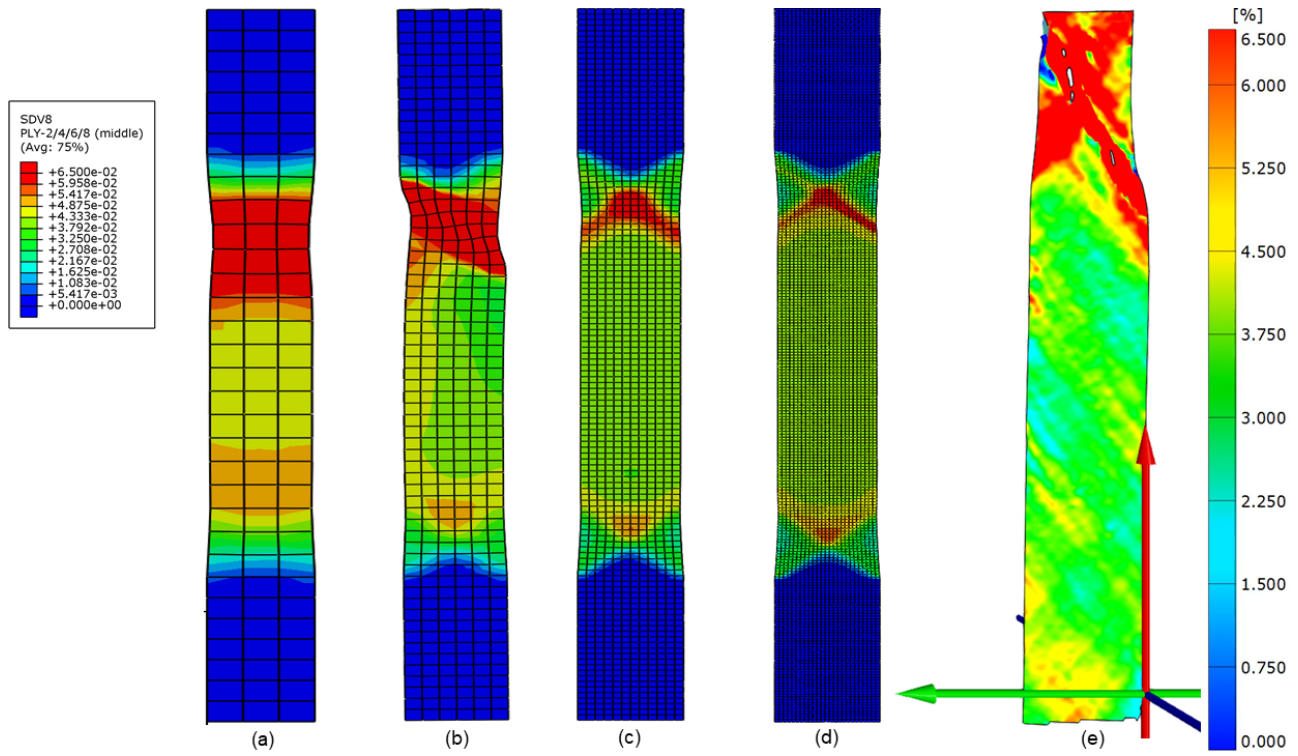


Figure 7.13: Illustration of shear strain fields ( $\gamma_{xy}$ ) at point D for: a) 8 mm x 8 mm, b) 4 mm x 4 mm, c) 2 mm x 2 mm, d) 1 mm x 1 mm mesh densities and e) DIC surface contour.

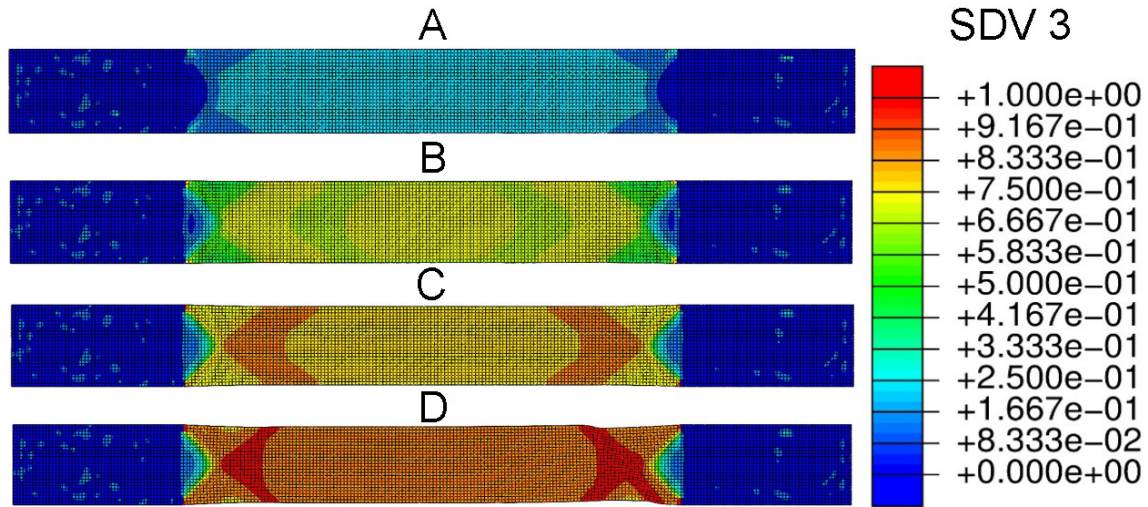


Figure 7.14: Failure index evolution for 1 mm mesh at discrete points of the shear stress-strain curve.

## 7.4 Compression loading

The numerical analysis conducted for the compression test, includes the modelling of the  $[0^\circ/90^\circ]_{4S}$  lay-up configuration and the cases with deliberate in-plane misaligned blankets ( $\phi_I = 2^\circ, 4^\circ, 6^\circ, 8^\circ$  and  $10^\circ$ ) and the corresponding  $\theta_I$ . Due to the nature of the failure modes



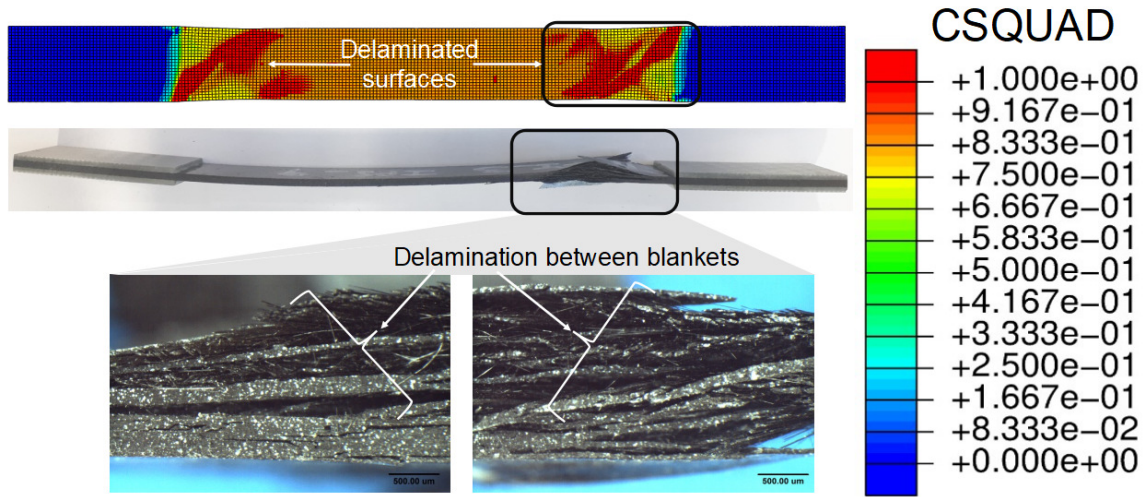


Figure 7.15: *Interface failure at the midsection of in-plane shear specimen (between blankets 4 and 5) of 1x1mm FE model is compared against tested coupon.*

(i.e. delamination, possible under end-tab failure) during compression test, it was decided to proceed with a full specimen model, including the end-tabs and cohesive zones. The compression numerical models are compared against specimens C-0-2, C-2-4, C-4-4, C-6-3, C-8-2 and C-10-3, hence their dimensions are defined as presented in Tabs. A.3 - A.8 in Appendix A.2. The material properties for the numerical analysis in compression are presented in Tab. B.6 of Appendix B.1.4.

### 7.4.1 Boundary conditions

Boundary conditions were modelled according to the Imperial College compression test method [137] fixture and they are illustrated in Fig. 7.16. Specifically, the specimen is loaded purely on the ends ( $F$ ) while a portion of the load is transmitted by shear via the end-tabs, thus lowering the average stresses at the end of the specimen. Longitudinal translational constraint ( $U_x = 0$ ) is applied at the end, opposite to the applied load,  $F$  (2 mm/s). A pressure load,  $P$  of 36 MPa is applied on each of the top end-tabs, simulating the clamping force created by two M10 bolts at each grip block of the fixture. The clamping pressure is obtained using the formula correlating the bolt clamping force,  $L$  with the tightening torque,  $T$  (15 Nm),  $T = K \cdot D \cdot L$  [215], where  $D$ , is the nominal bolt diameter and  $K$ , the torque-friction coefficient equal to 0.20. The translational constraints in the z-axis correspond to the reaction at the wall of the fixture generated by the applied pressure. Translational constraints across the specimen

( $U_y = 0$ ) are applied to the one side of the specimen with the opposite side to be set free for translation in the y-axis, as it is the constraint condition in the actual test fixture.

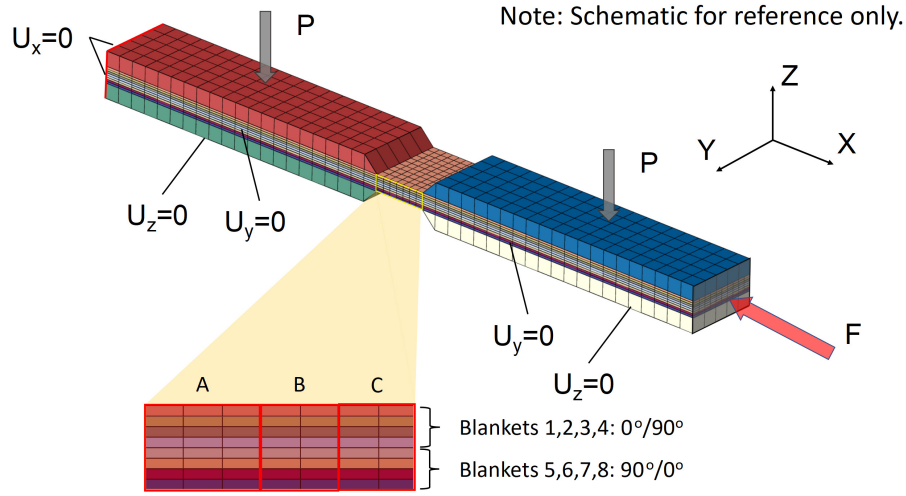


Figure 7.16: Illustration of boundary conditions and partitioning of the compression specimen.

### 7.4.2 Analysis parameters

In this FE model, it was considered essential to proceed with a combined sensitivity analysis, involving mesh density variation and implementation of different number of cohesive zones. The objectivity study is performed for the case of  $[0^\circ/90^\circ]_{4S}$  without deliberate in-plane misalignment,  $\phi_I=0^\circ$ , involving mesh density variation and different number of CZs. Four cases are simulated: 2 mm mesh density: i) without CZ and ii) using 3 CZs; 1 mm mesh density with: iii) 7 and iv) 15 CZs. The CZs in all related cases were symmetrically allocated through the thickness of the specimen.

| Simulations   | Element size | # Elements | Layers / Element (IP) | Cohesive Zones |
|---------------|--------------|------------|-----------------------|----------------|
| LC_2x2        | 2x2          | 7240       | 4                     | -              |
| LC_COH_2x2    | 2x2          | 7240       | 4                     | 3              |
| LC_COH_7_1x1  | 1x1          | 16240      | 2                     | 7              |
| LC_COH_15_1x1 | 1x1          | 16240      | 1                     | 15             |

Table 7.4: Summarised mesh properties of the numerical models for compression.

The interface properties of the cohesive zones (CZ) between the blankets were characterised using a "hard" normal contact criterion in order to restrict penetration of the elements of each blanket to the neighbour, and a cohesive behaviour, which was defined by the interface

properties presented in Chapter 6. Also, the values of the interlaminar fracture toughness were selected to be at the initiation stage ( $G_{IC_{Init}}$ ,  $G_{IIC_{Init}}$ ). The reason for this choice regards the fact that the length of the gauge region of the compression specimen is less than 10 mm (i.e., 8 mm) and hence steady state delamination cannot be achieved. In a similar manner, the interface properties between the end-tabs and the outer NCF blankets are modelled, modifying the values of the cohesive terms according to the Huntsman Araldyte 2011 [162] adhesive mechanical properties.

Moreover, simulations were carried out for the compression tests with  $(\phi_I)$  equal to  $2^\circ$ ,  $4^\circ$ ,  $6^\circ$ ,  $8^\circ$  and  $10^\circ$ , and compared against FE analyses without the novel terms of Eq. 5.32. Additionally, simulations of models with average values for misalignment across all layers and regions were performed. For all these cases the 1 mm element size with 7 CZs modelling approach was applied and 8 elements through-the-thickness were implemented, one for each NCF blanket. Similarly, the CZ between the end-tabs and the outer NCF blankets are modelled according to Huntsman Araldyte 2011 [162] adhesive mechanical properties. The end-tabs were modelled with continuum shell elements (SC8R) introducing the elastic properties of Glass-Fibre [216]. The values of  $\phi_I$  and  $\theta_I$  introduced in the numerical models, for each section and layer of the specimen (Fig. 7.16) are summarised in Tabs. B.12 to B.23 of Appendix B.2.3, while the average values of misalignment, can be found in Tab. B.24.

### 7.4.3 Results

The stress-strain response of the sensitivity analysis is presented in Fig. 7.18 and is in good agreement with the experimental data for the different configurations. In particular, the linear behaviour of the tested specimen was predicted accurately, proving that the applied stiffness knock-down factor performed well. Indicatively, in Tab. 7.5 the calculated values of the KD factor are summarised for the compression specimen without deliberate in-plane fibre misalignment. The KD values are expressed with respect to the local values of  $\phi_I$  and  $\theta_I$  per section per blanket as described in Eq. 5.18 and quantify the influence of the fibre misalignment to the reduction of the longitudinal stiffness. In Fig. 7.18, for strains of 0.004 onwards, a discrepancy

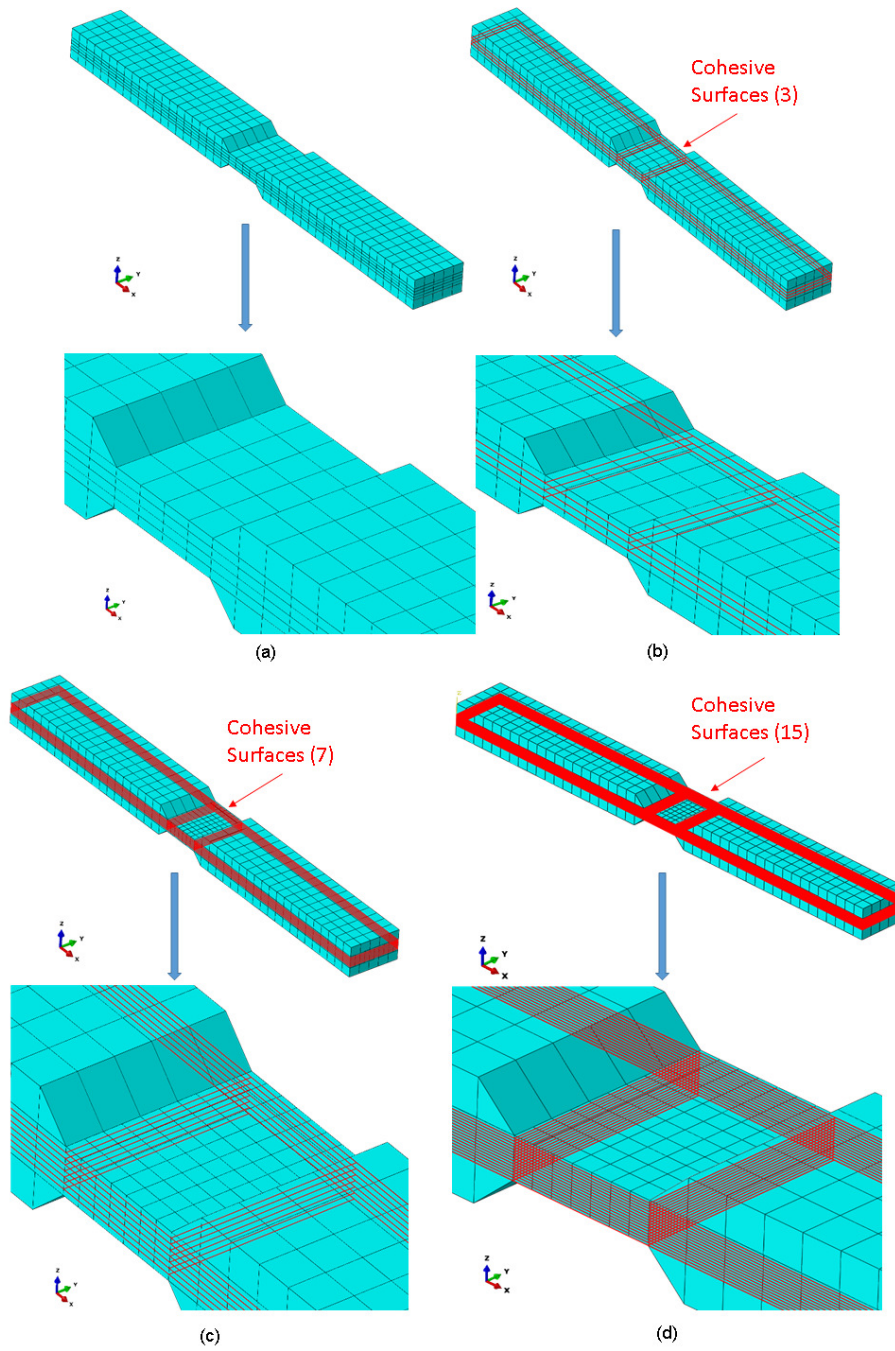


Figure 7.17: *Illustration of the mesh density and CZ sensitivity for compression FE models: a) 2 mm x 2 mm - No CZ, b) 2 mm x 2 mm - 3 CZ, c) 1 mm x 1 mm - 7 CZ and d) 1 mm x 1 mm - 15 CZ.*

in the non-linear region is noted between the FE results and the experimental behaviour; this could be attributed either to manufacturing imperfections within the specimen such as voids, or to additional stiffness degradation of the  $0^\circ$  layers if higher values of fibre misalignment exist within the specimen which could not be observed or named optically. Ultimate failure was overestimated by the FE models, ranging from 10%, for the 2 mm mesh density without CZs

configuration, to 2.3% when 1 mm mesh density with 15 CZs was applied. The undulations of the plots in Fig. 7.18 for strain values higher than 0.006 are due to the mass scaling of the FE analyses.

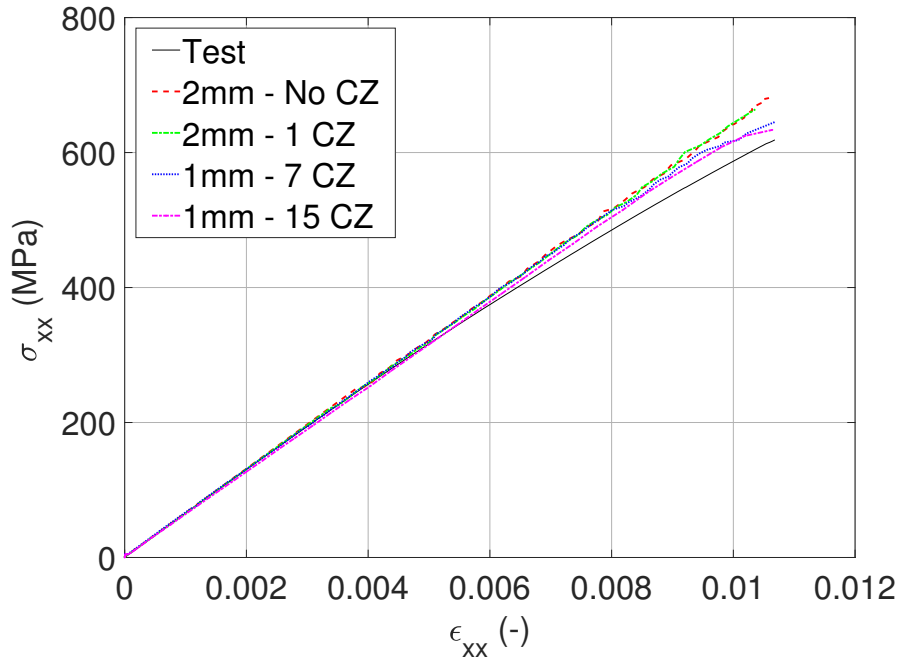


Figure 7.18: *Mesh and CZ sensitivity in stress-strain relationship of  $[0^\circ/90^\circ]_{4S}$  compared with test results.*

In Fig. 7.19, the cohesive zone failure index of the FE models for 3, 7 and 15 CZs is presented. The illustrations were taken at strain equal to 0.0095 where interface failure index reached unity, prior to ultimate failure. A similar delamination pattern is revealed for all three configurations. In the 3 CZ model, decohesion was initiated from the two outer NCF blankets at the  $90^\circ/0^\circ$  interface (Fig. 7.19 - left, point A) and then propagated at the midplane of the specimen (Fig. 7.19 - left, point B), therefore ultimate failure did not occur simultaneously with delamination.

For the 7 and 15 CZs configurations, unlike the 3 CZ model, the amount of separation observed is not expanded along the gauge length (8 mm) of the specimen (Fig. 7.19 middle and right) but it is limited to 5 mm and 4 mm respectively. In particular, in the 15 CZs model it is noted that the interlaminar failures are further localized to the region where fibre kinking and matrix failure occurs. Moreover, decohesion due to peel is identified between the outer layers of the specimen (Fig. 7.19 - middle, point C and Fig. 7.19 - right, point E). Interlaminar shear failure is again predicted at the mid-section of the specimens and the neighbouring CZs (Fig. 7.19 -

| Blanket | Section    |        |        |        |
|---------|------------|--------|--------|--------|
|         | A          | B      | C      |        |
| 1       | $\phi_I$   | 1.2    | 1.8    | 2.3    |
|         | $\theta_I$ | 2      | 2.6    | 3.2    |
|         | KD         | 0.9862 | 0.9693 | 0.9506 |
| 2       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 1.7    | 2      | 1.9    |
|         | KD         | 0.9763 | 0.9674 | 0.9628 |
| 3       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 3.1    | 2.1    | 1.8    |
|         | KD         | 0.9415 | 0.9652 | 0.9647 |
| 4       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 3.4    | 1      | 3.5    |
|         | KD         | 0.9319 | 0.9834 | 0.9193 |
| 5       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 2.5    | 3.1    | 2.2    |
|         | KD         | 0.9586 | 0.9386 | 0.9564 |
| 6       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 2.3    | 1.6    | 2.2    |
|         | KD         | 0.9636 | 0.9750 | 0.9564 |
| 7       | $\phi_I$   | 1.4    | 1.65   | 2.1    |
|         | $\theta_I$ | 1.8    | 2.5    | 1.1    |
|         | KD         | 0.9744 | 0.9556 | 0.9756 |
| 8       | $\phi_I$   | 1.8    | 1.5    | 1.9    |
|         | $\theta_I$ | 1.4    | 2.9    | 3.6    |
|         | KD         | 0.9762 | 0.9464 | 0.9189 |

Table 7.5: *Summary of the calculated values of the KD factor at each section per blanket, as a function of the in- ( $\phi_I$ ) and out-of-plane ( $\theta_I$ ) fibre misalignment, for the compression specimen without deliberate in-plane fibre misalignment.*

middle, point D and Fig. 7.19 - right, point F). The discrepancy between the different number of CZ models in predicting interlaminar failure, can be attributed to the fact that the relative slippage between the layers is smeared between the NCF layers through the thickness of the specimen for a high number of CZs, which is expressed as a slight non-linearity in the stress - strain diagrams, whereas for FE models with fewer CZs, this relative motion only occurs to the first available CZ, concentrating higher amount of failure energy. Comparing the delaminated predicted areas of the 15 CZ model with the experimental observation of Fig. 4.27 a good qualitative agreement can be observed.

The damage sequence of the four FE models is illustrated in Fig. 7.20 (a, b, c, d), which depicts that the shear driven intralaminar failure of the 90° layers was predicted to be the first



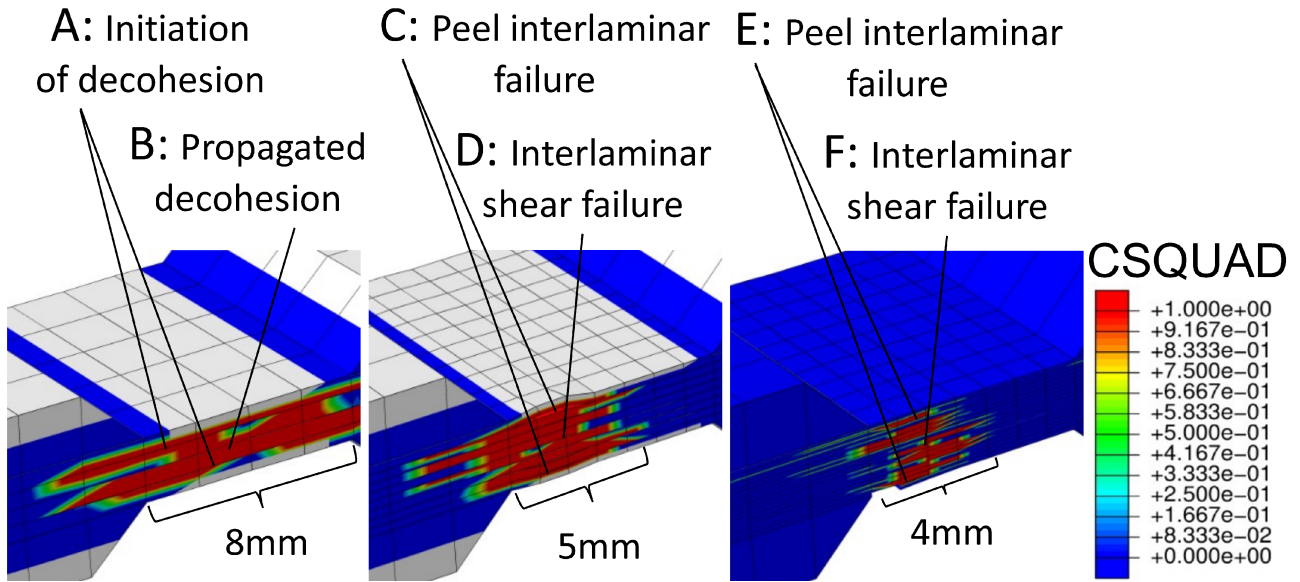


Figure 7.19: *Interface failure in compression for 3 (left), 7 (middle) and 15 (right) CZs at strain equal to 0.0095.*

damage event, initiated at the midsection of the specimen and gradually propagated to the 90° layers towards the surface of the specimen. The 0° layers exhibited fibre kinking failure which led to the ultimate compressive damage as illustrated in Fig. 7.20 (e, f, g, h). The maximum compressive stress is reached and the kink-band is formed as all support is lost from the surrounding matrix material (decohesion) resulting in a Mode I delamination.

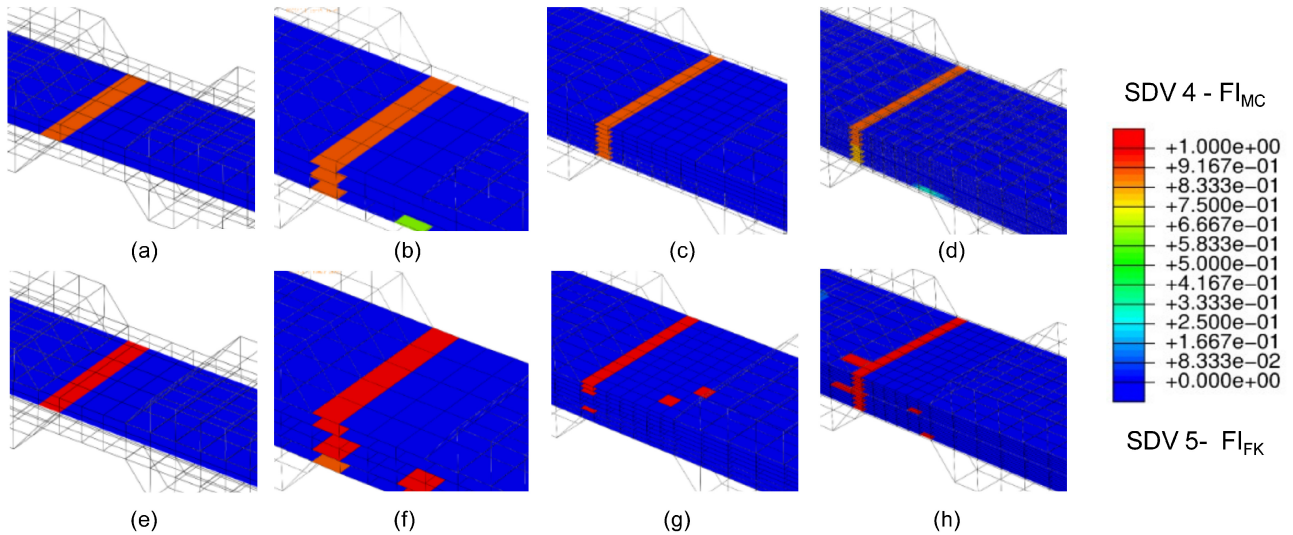


Figure 7.20: *Illustration of damage sequence for the different mesh densities and CZs FE models. Matrix failure: a) 2 mm - No CZ, b) 2 mm - 3 CZs, c) 1 mm - 7 CZs, and d) 1 mm - 15 CZs. Fibre kinking: e) 2 mm - No CZ, f) 2 mm - 3 CZs, g) 1 mm - 7 CZs, and h) 1 mm - 15 CZs.*

In Fig. 7.21, the numerical results for the compressive strength,  $X_C$  and longitudinal modulus,  $E_{xx}$  for different values of  $\phi_I$  and  $\theta_I$  are presented and compared against the test results. The results of the simplified numerical modelling (averaged values of fibre misalignment) are described with indication "Avg", whilst the detailed numerical models with specifically assigned fibre misalignment are indicated with the subscript "Spec". In principle, the FEA predictions for "Spec" simulations appeared to be slightly more optimistic for both magnitudes when compared with the mean experimental results but still well within the margin of the experimentally recorded values. Specifically,  $E_{xx}$  was overpredicted in the range of 2.1 - 3.5% whilst  $X_C$  discrepancy varied between 2.5 - 4.2%. This was expected since a significant amount of uncertainty exists in the internal structure of the specimens not only in terms of in- or out-of-plane fibre misalignment but also as voids or other manufacturing defects, not accounted by the present constitutive model. Moreover, instead of the maximum values of  $\phi_I$  and  $\theta_I$ , the mean values were introduced. As a consequence of this approach, possible premature failures of the actual specimen in those areas where the maximum fibre misalignment existed, could not be predicted. Notwithstanding the aforementioned observations, the FEA models captured successfully the trend with increasing  $\phi_I$  of both the elastic modulus and compressive strength in compression. Concerning the "Avg" numerical model predictions, compressive strength and modulus are overestimated by 2 - 4.3% against the detailed numerical model. An explanation for this outcome is that the homogenisation of the angles eliminates the effect of excessive fibre waviness in the sections and layers of the specimen which would result in locally reduced stiffness and strength and thus to early failure of certain elements. Also, it worth mentioning that with the increasing values of in-plane misalignment, the deviation in stiffness and strength prediction is increasing too, starting from 2.2% for  $0^\circ$  in-plane misalignment up to 4.7% for  $10^\circ$ . Although, the discrepancy is not prohibitively high when compared against the test results and the detailed numerical model, the simplified configuration lacks the capability to predict localised damage due to fibre undulation and forecast the failure sequence.

The contribution of each term of Eq. 5.32 to trigger kinking failure under pure compression ( $\sigma_{11} < 0$ ), was also investigated, with particular focus on the newly added terms C and E. A side-by-side comparison was performed between numerical models with full version of Eq.



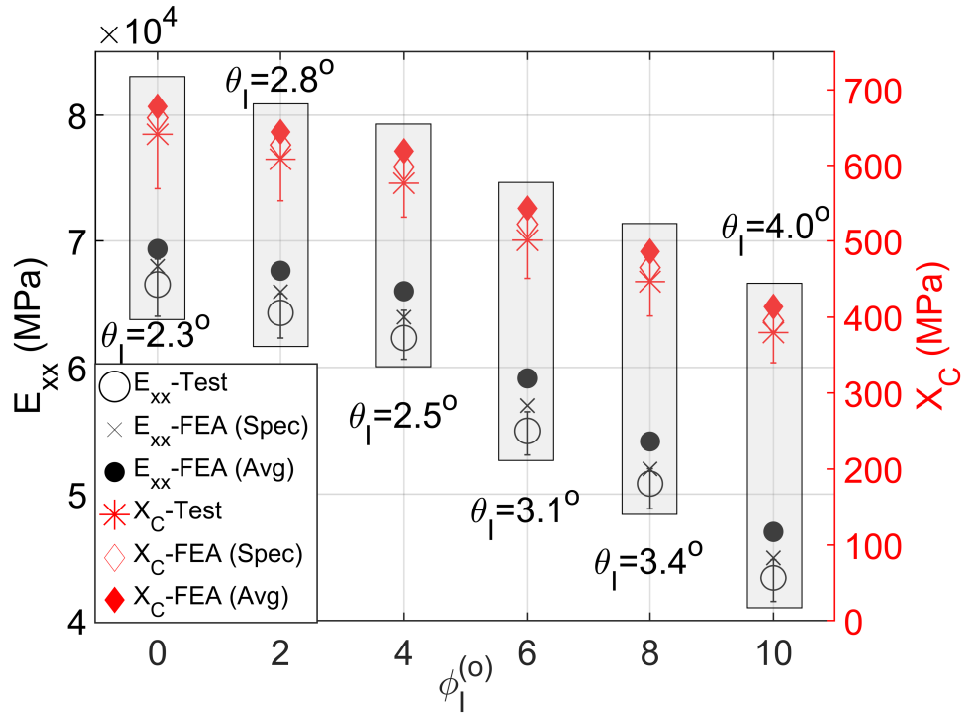


Figure 7.21: *Compressive behaviour for different values of  $\phi_I$  and  $\theta_I$ . Compression strength and longitudinal modulus predictions of the specific (Spec) and simplified (Avg) numerical models against test results.*

5.32 (terms C and E included) and without the novel terms. In the absence of terms C and E the predictive compressive strengths for varying in- and out-of-plane fibre misalignment is essentially higher by 12 to 20% than when these two terms are included as shown in Fig. 7.22. Specifically, the deviation becomes higher with increased fibre misalignment. This discrepancy is mainly attributed to the absence of term C, which is susceptible mainly to  $\phi_I$  and the longitudinal compressive stress ( $\sigma_{11} < 0$ ), whereas term E is more sensitive to  $\theta_I$  and transverse tensile stress ( $\sigma_{22} > 0$ ).

Based on the predicted compressive strength for both configurations as presented in Fig. 7.22, the corresponding FIs and the significance of each term of Eq. 5.32 is depicted in Fig. 7.23, in which the evolution of each term (i.e., A, B, C, D and E) of Eq. 5.32 is quantified for different  $\phi_I$  and  $\theta_I$  values. Specifically, in Fig. 7.23 at each pair of stacked bars, the bar on the left corresponds to the FI of Eq. 5.32 with terms C and E included, whereas the bar on the right side with those two terms omitted. Hence, it is illustrated for both cases the contribution the terms of Eq. 5.32 so that the FI reaches unity (i.e. failure onset is triggered). Term A appears to be proportional to  $\phi_I$ , while it has a small influence in damage initiation (0 - 0.033) since

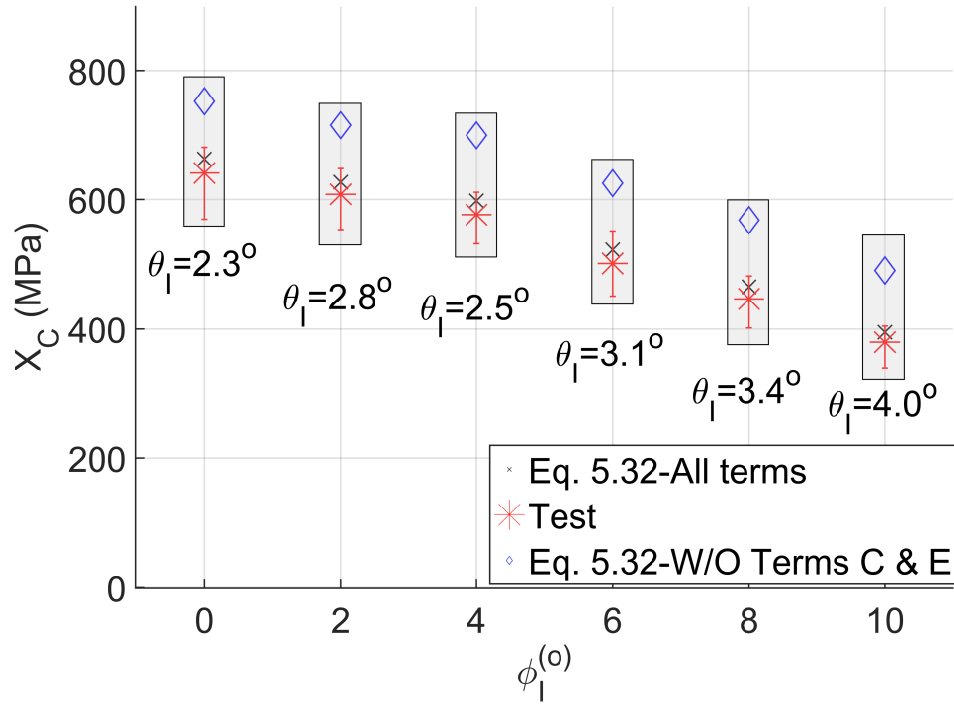


Figure 7.22: Compression strength predictions against test results for different values of  $\phi_I$  and  $\theta_I$  as predicted by Eq. 5.32 with and without terms C and E.

it is primarily dependent on the transversely applied loads. It is apparent though that for the configuration without terms C and E, term A is more essential by obtaining almost twice the values of the full Eq. 5.32 criterion. On the other hand, term B and the newly added term C appeared to be the dominant contributors so that the failure index in fibre kinking reaches unity. At this stage, it becomes noticeable that the absence of term C in the simplified version causes the overestimated compressive strength, because although term B reaches unity, that occurs for higher compressive stress. Term D is proportional to both  $\phi_I$  and  $\theta_I$ , hence its contribution to the failure index is negligible for  $\phi_I$  ranging from  $0^\circ$  to  $4^\circ$  (0 - 0.019), while for angles from  $6^\circ$  to  $10^\circ$  it gradually obtains stronger impact (0.06 - 0.21).

As in term A, so in term D, the index values are twice as much for the simplified version of Eq. 5.32, which implies that the contribution of term E is smeared within terms A and D. The newly added term E due to the limited change of  $\theta_I$  exhibits a relatively constant behaviour and, as expected, since no external transverse loads are applied, the influence to the failure index is low. Thus, term E does not crucially affect the prediction capabilities of the simplified version of Eq. 5.32. The conclusion from this comparative study, highlights the significance

of primarily term C in Eq. 5.32 under pure compression, which undoubtedly improves the prediction capability of the failure criterion.

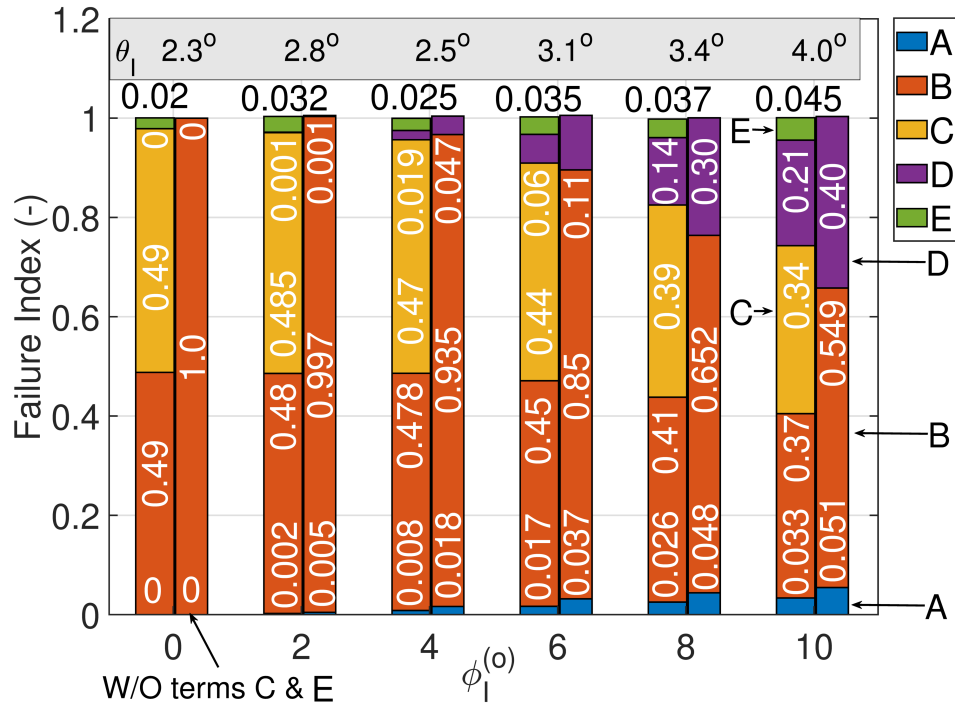


Figure 7.23: *Decomposition of fibre kinking failure index. All five terms of Eq. 5.32 are active (stacked bars on the left) and terms C and E are neglected (stacked bars on the right).*

# Chapter 8

## Compact Tension Numerical Analysis

### 8.1 Introduction

The capability of the proposed model to predict not only the damage onset, but also the damage propagation is evaluated through the numerical study of the CT case. Specifically, the contribution of the newly introduced exponential unloading law during element failure in tension as well as the influence of the interactive damage propagation formulation between transverse tension and shear stresses presented in Chapter 5 of the thesis were studied.

With regards to the prediction of the translaminar fracture toughness of composites by other researchers, an analytical model was developed by Pimenta and Pinho [182] which accounted for the principal energy dissipation mechanisms, namely fibre pull-out and matrix-bundle debonding, and introduced a stochastic variation to define the length of the fibres. The model reproduced well the test results from the literature for different types of fibre and matrix. Extensive research has been performed by Ortega et al. [217–219] who also developed analytical models focusing on cohesive modelling and on inverse methodology. With the use of an interpolating function the crack opening of an isotropic and an orthotropic material is predicted. The disadvantage of this approach is that it does not adopt a predictive character in terms of the orientation of the crack evolution, since it assumes that the crack evolves along the symmetry

plane of the specimen and perpendicular to load direction, while this might not be the case for quasi-isotropic lay-up configurations with excessive matrix damage or for specimens with structural imperfections. Moreover, the inverse methodology presented in [218] to characterise the cohesive law, requires several points to be extracted from the load-displacement curve of a CT test.

## 8.2 Analysis parameters

The analytical formulations and the constitutive failure criteria presented in Chapter 5 were introduced to Simulia Abaqus/Explicit as a VUMAT material model and validated against the test results presented in Chapter 3. Continuum shell elements (SC8R) were utilised to model the NCF blankets of the specimens, while each layer of the NCF blanket was modelled with 3 integration points through-the-thickness. The fibres within the layers were homogenised and assumed to be evenly distributed. Fibre misalignment is introduced at layer level directly to the material card model as explained in Chapter 6 based on the OM observations for the CT specimens. In Tabs. B.25 and B.26 of Appendix B.2.4, the recorded in-plane and out-of-plane fibre misalignment angles per blanket and partition of the CT specimen are summarised, while the partitions of the specimen are illustrated in Fig.8.1. Also, the material properties introduced to the material card for this analysis can be found in Tab. B.6 of Appendix B.1.4.

For the accurate estimation of crack propagation it is necessary to input the translaminar fracture toughness ( $G_{ICT}$ ) values as obtained from the mechanical testing. Based on the fractography, the experimental R-curves and DIC post-processing, it is opted to assign the translaminar fracture toughness of initiation,  $G_{ICT_{Init.}}^{0o}$  to the elements of the crack region for the first 3 mm whereas for the remaining crack length the R-curve propagation mean value,  $G_{ICT_{Prop.}}^{0o}$  is utilised (Fig. 8.1). The numerical model's crack tip is inherently infinitesimally sharp, meaning that the experimental microcracks formed during the crack initiation could not be predicted numerically. Moreover, the Abaqus cohesive surface approach explained in Chapter 6 is used to model the CZ between the NCF blankets. The global in-plane mesh size was set to 2.0 mm, however the highlighted area of Fig. 8.1 (middle), is critical for failure onset and propagation,

therefore, around and along the expected crack path, refined mesh is assigned. Through-the-thickness mesh density is equal to the cured NCF blanket thickness, namely 0.275 mm. Like in the in-plane numerical cases, because of the applied high loading rates (i.e. 2 mm/s) in the FE analyses, Rayleigh damping was introduced to minimise the generation of unreal modes and to ensure that the kinetic energy/internal energy ratio of the system will not exceed the order of 0.1. Also, no artifice was used to initiate failure at any specific location or along any certain direction.

### 8.3 Boundary conditions

Boundary conditions are representative of the actual testing constraints. Nodes A and B as shown in Fig. 8.1 are the constraint points of the specimen. For node A, four out of six degrees of freedom are constrained. Specifically,  $U_X = U_Z = R_X = R_Y = 0$  but  $U_Y = F = 2.0\text{mm/sec}$  which is the displacement rate towards positive “Y” direction. On the other hand, node B is constrained in five degrees of freedom, however the unconstrained degree is  $R_Z$  ( $R_Z \neq 0$ ). Multiple point beam constraints are assigned to nodes A and B, substituting the load and support pins respectively.

## 8.4 FE Results

### 8.4.1 Mesh and CZ sensitivity analysis

Prior to the final set-up of the numerical model, an objectivity analysis is employed firstly to define the optimal mesh size in the critical region where crack is expected to initiate and propagate, and then to examine the sensitivity of FE results to the number of cohesive zones. Five CT FE models are generated with different element sizes and number of cohesive zones introduced between the blankets. Configuration (A) corresponds to refined mesh size of 2 mm and no CZ and is set as the benchmark, whereas the other four configurations are characterised by increased number of CZs and three discrete element sizes. Table 8.1 summarises the examined

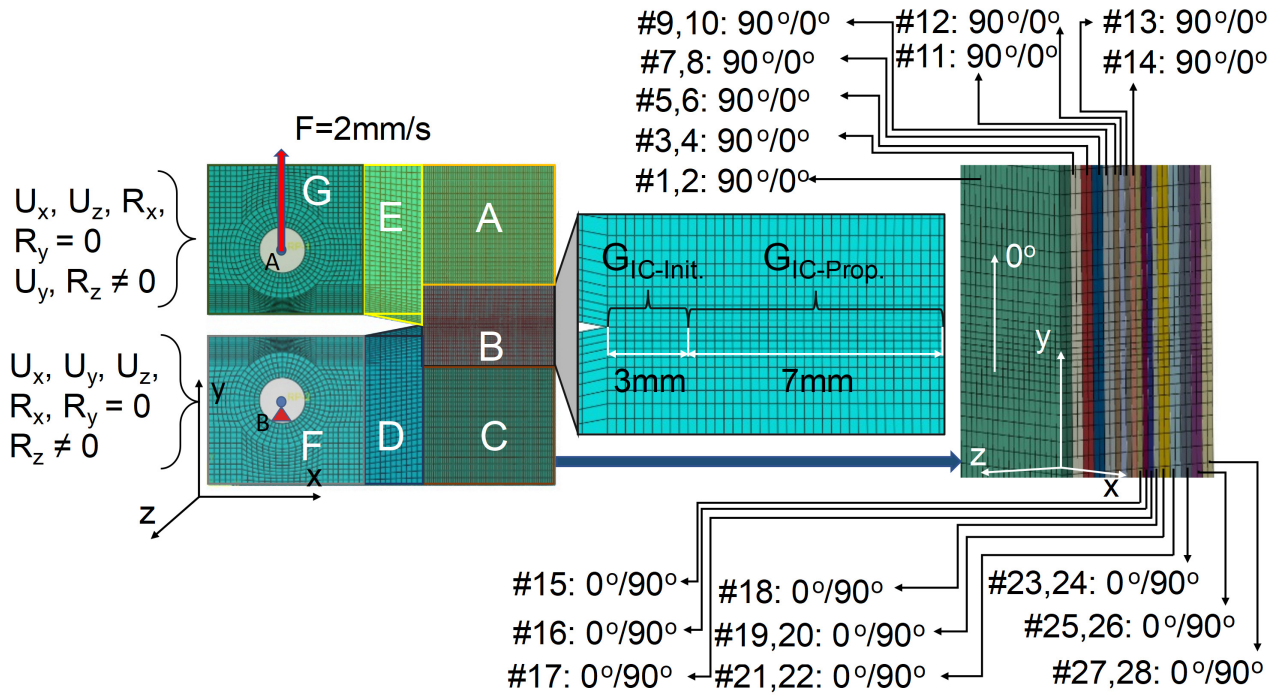


Figure 8.1: CT specimen boundary conditions and partitions (left), area of refined mesh (middle) and lay-up illustration (right).

sensitivity cases.

| Configuration | N° of CZ | Refined element size (mm) |
|---------------|----------|---------------------------|
| A             | 0        | 2x2                       |
| B             | 1        | 1x1                       |
| C             | 9        | 0.5x0.5                   |
| D             | 17       | 0.3x0.3                   |
| E             | 27       | 0.3x0.3                   |

Table 8.1: Configurations of CZ number and mesh size for the objectivity study.

With regards to the distribution of the CZs through the specimen, based on the findings of the CT fractography, as critical areas for decohesion are identified to be primarily the midsection and then the interface between NCF blankets towards the outer surfaces of the specimen. The CZ are allocated symmetrically to the midplane of the specimen for each FE model and presented in Fig. 8.2, highlighted as the thick beige lines, placed at  $90^\circ/0^\circ$  interfaces.

In Fig. 8.3 the mesh sensitivity analysis is presented through the resulting load-displacement curves. It can be observed that during the elastic region, namely displacements from 0 to 3 mm, no major difference is identified between the configurations. However, from 3 mm

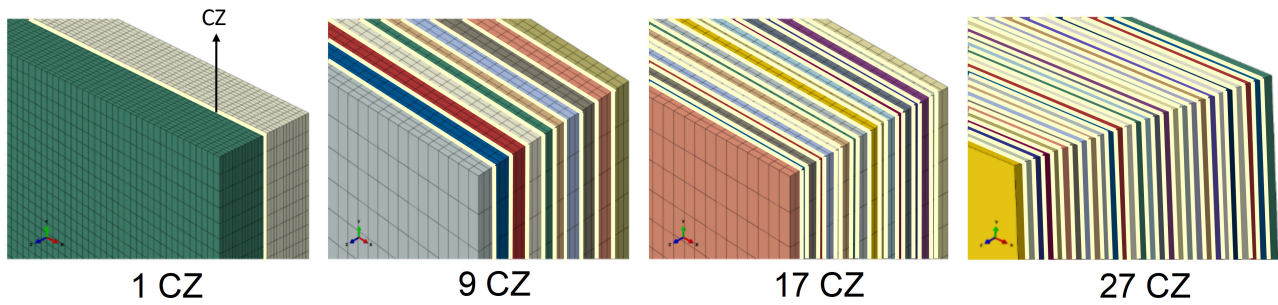


Figure 8.2: *Illustration of CT specimens with CZ (beige highlighted lines) in FE model. a) Mid-plane CZ, b) 9 CZ, c) 17 CZ and d) 27 CZ.*

until failure onset, where non-linearity is exhibited due to matrix cracking and elementary interlaminar shear phenomena, a sensitivity to interface properties is noted. In particular, whilst FE model A appears to have stiffer response when compared against models B, C, D and E with introduced CZs, the latter models, do not present significant deviations with each other until failure initiation. As failure initiation is considered the first significant load drop of the load-displacement curve and the experimental reference value was found at Section 2 of this paper to be 9.43 kN. The critical load prediction appears to be more susceptible to mesh size rather than the number of CZ, since model A has a 10% discrepancy when compared to the test results whereas models D and E which are assigned the same element size (0.3 mm) with different number of CZs allocated (17 and 27 respectively) exhibit only marginal difference (2.8 and 3.7% respectively). In terms of damage propagation, element size does play an important role since the failure of larger elements corresponds to greater load drops at higher displacement values. Table 8.2 summarises the results of the sensitivity analysis, namely maximum load at damage onset and corresponding displacement, which are compared with the test results. In principle, it is noted that the coarser mesh and reduced number of CZs predicts onset failure in higher load and displacement values. Convergence is identified between models D and E, hence, accounting also the computational expense, configuration D is opted to proceed with the final FE analyses.



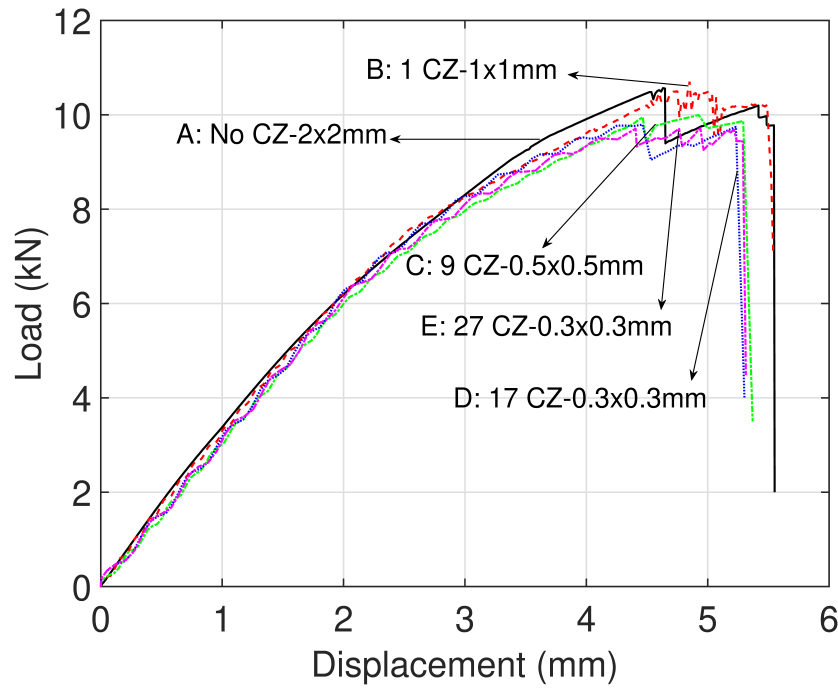


Figure 8.3: Load - displacement curve for the objectivity analysis models.

|      | $L_C$ (kN) | CV (%) | $\delta_{fail}$ (mm) | CV (%) |
|------|------------|--------|----------------------|--------|
| Test | 9.43       | -      | 4.30                 | -      |
| A    | 10.47      | 10 %   | 4.60                 | 6.5 %  |
| B    | 10.30      | 8.5 %  | 4.56                 | 5.7 %  |
| C    | 9.88       | 4.6 %  | 4.43                 | 2.93 % |
| D    | 9.79       | 3.7 %  | 4.41                 | 2.6 %  |
| E    | 9.70       | 2.8 %  | 4.40                 | 2.3 %  |

Table 8.2: Summarised results of failure onset load and the corresponding displacement for the objectivity analysis against test results.

#### 8.4.2 Comparison of Linear and Exponential unloading against experimental results

In Fig. 8.4a) the predicted response of the linear and exponential unloading models is compared against the load - displacement test curve. The experimental curve is obtained as the smoothed average of the tested specimens. Both models correlate well with the experimental data at the elastic region (0 - 3.7 mm), whereas the maximum load is overestimated by both configurations by  $\sim 3.5\%$ . This behaviour is no surprise since the same failure criteria are implemented in these two models. With regards, though, to the load response during crack opening, it is apparent in Fig. 8.4aa) that the exponential unloading model matches well with the experimental curve.

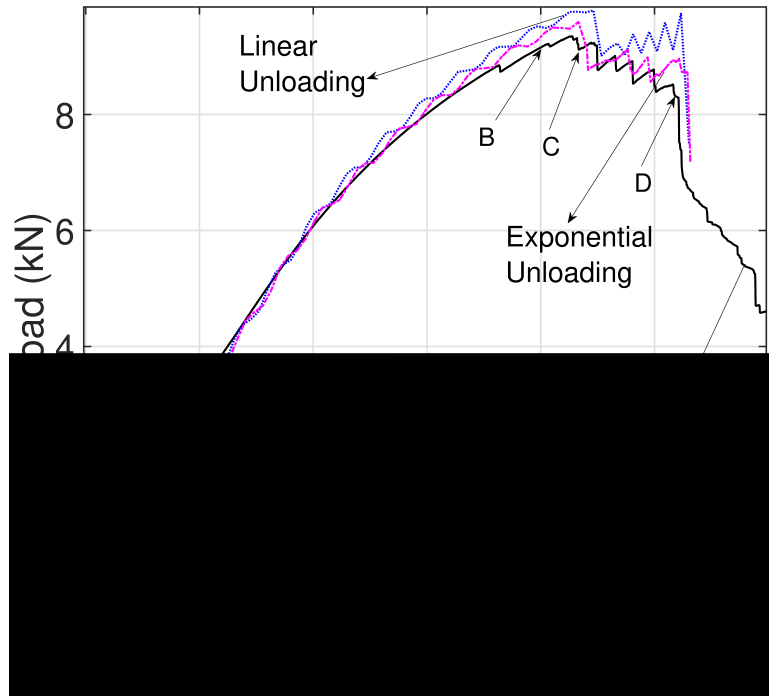
This is attributed to that fact that the proposed exponential unloading law assumes the crack evolution as a gradual development of a softening bridging zone and it seems sufficient to account for the multiple damage mechanisms that occur during stable fracture, hence a gradual load reduction is observed. On the other hand, the linear unloading law significantly deviates from the experimental data during crack opening exhibiting a rising load response.

These load responses are further extended, through Eq. 4.6, to the R-curve predictions (Fig. 8.4b)). The translaminal fracture toughness of initiation was predicted by both models with marginal discrepancy of 2.3% (43 N/mm - linear) and 1.2% (42.5 N/mm - exponential) against test result (42 N/mm). Although until 3 mm of crack opening the linear model is in good fit with the test data, from that point onwards a linearly increasing trend of fracture toughness is predicted, due to the rising load shown in Fig.8.4a. Unlike the linear model, the average fracture toughness by the exponential law (75.5 N/mm) is reproduced with good precision ( $\sim 5\%$ ) when compared with the respective experimental value (70.85 N/mm). The response of the exponential law can be further improved if variable toughness is provided.

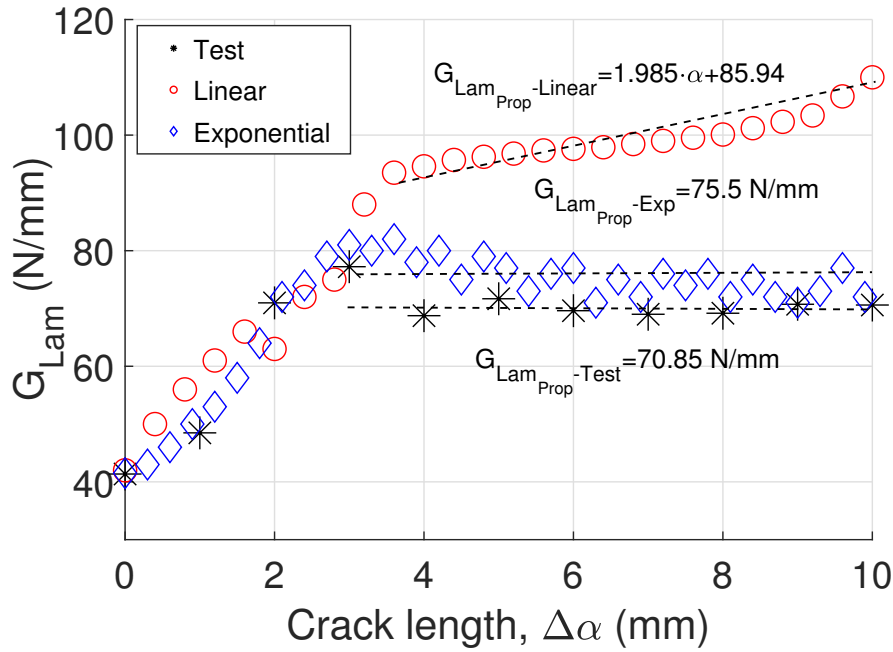
### 8.4.3 Qualitative comparison between FE and experimental results

In addition to the previous observations, a supplementary qualitative comparison in terms of strain mapping of the  $\epsilon_{yy}$  is employed. In Fig. 8.4a) four discrete points are assigned along the load versus displacement curve indicating four stages during CT test. Specifically, point A corresponds to the initial condition prior to load application, point B represents the stage before crack onset ( $\delta = 4.0$  mm), point C is situated in the crack propagation region ( $\delta = 4.6$  mm) and finally point D captures the compressive failure ( $\delta = 5.2$  mm) which occurs at the rear end of the specimen.

In Fig. 8.5 the exponential law FE model is compared with the DIC strain map of CT specimen for the four discrete stages (A, B, C and D). The x and y axes regard the global coordinates and not the material coordinate system. Also the surface layer observed is at  $90^\circ$  to the fibre direction, namely parallel to the x-axis. The FE strain fringe plots correlate very well with the DIC strain map for all stages. Specifically the strains at the critical areas around the notch tip



(a) Load - displacement response.



(b) R-curves response.

Figure 8.4: Comparison between the linear and exponential unloading models against the experimental data.

are captured accurately, as well as the compressive strains at the rear end of the specimen. The extended range of strain contour in the critical regions of tension and compression particularly for points C and D is due to the smearing methodology applied on the elements (section 4.4) meaning that the total strain is gradually distributed along the elements.

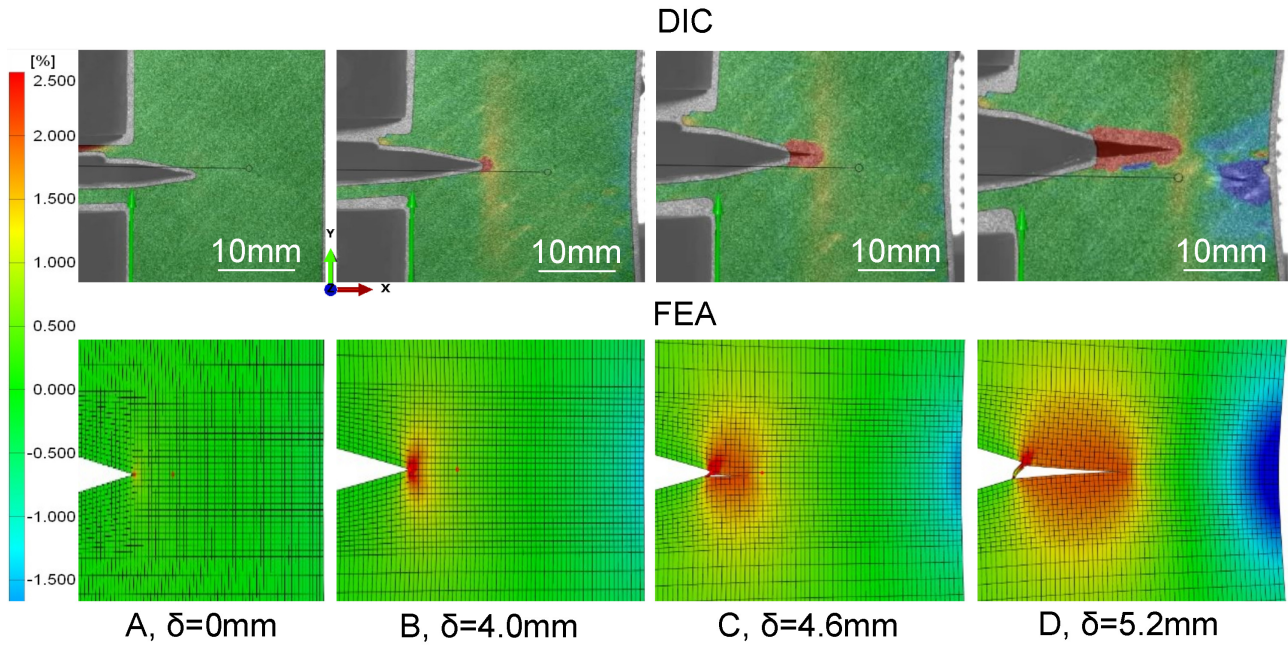


Figure 8.5: Strain map ( $\epsilon_{yy}$ ) comparison between DIC (top) and FE model (bottom) at  $\delta$ : a) 0 mm, b) 4 mm, c) 4.6 mm and d) 5.2 mm.

Furthermore, interlaminar failures predicted by the FE model are compared against ultrasound C-Scan observations. In this case, an interrupted CT test is performed and at each stage of the test the specimen is scanned through-the-thickness to detect potential blanket separation. The experimental study reveals that the interface between blankets 13 and 14 ( $0^\circ$ - $90^\circ$  layer interface) towards the midsection of the specimen is prone to delaminate. Fig. 8.6 illustrates the FE model and C-Scan findings at each stage. The FEA contours illustrate the development of damage between the blankets of the NCF composite through CSQUADSCRT, which is the damage variable of Abaqus for cohesive failure under the quadratic stress criterion. Damage is assumed to initiate when the criterion reaches unity. In Fig. 8.6 the damaged area between blankets 13 and 14 is represented with red colour and is directly compared to the C-Scan findings, in which the projected damaged area is depicted by dark blue hue (red circled regions). In the transition of point A to point B no delamination was observed, whereas between points B and C some small delamination is identified around the notch, which is further extended to the rear end of the specimen at point D, where compressive failure occurs. The previously described evolution of delamination is captured accurately by the FE model as indicated in Fig. 8.6 (bottom).

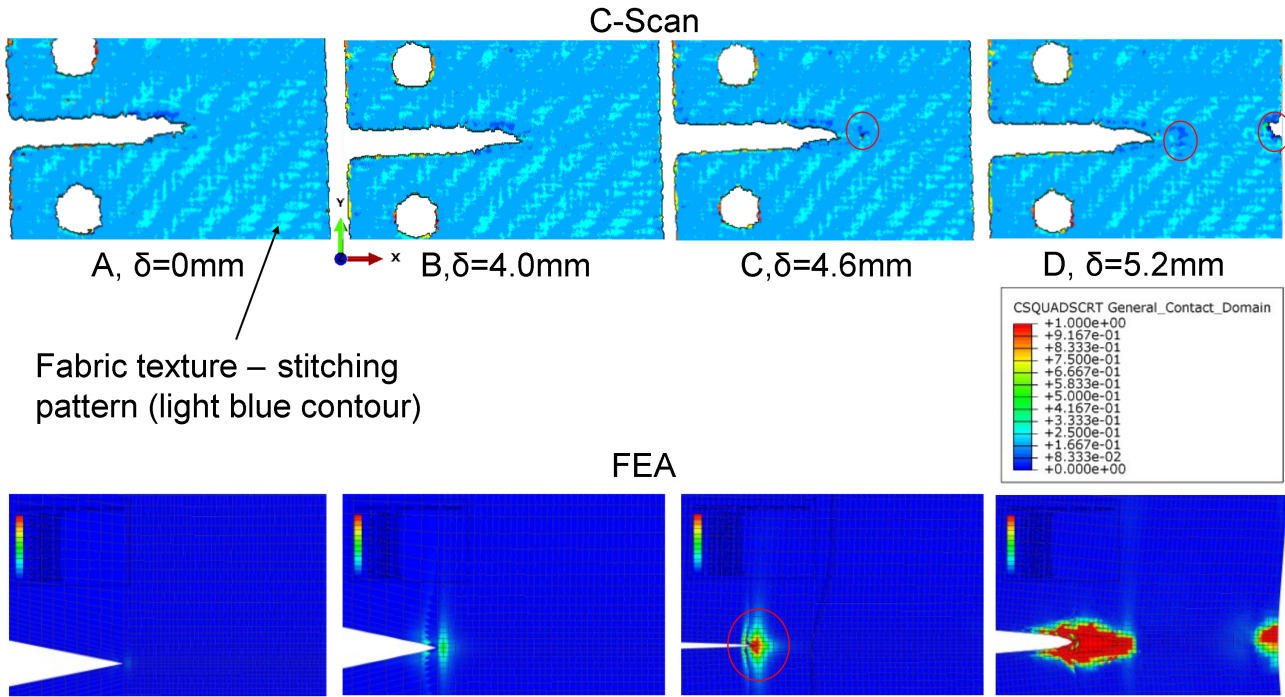


Figure 8.6: *Delamination inspection by ultrasonic C-Scan (top) compared with FE model (bottom) cohesive law FI at  $\delta$ : a) 0 mm, b) 4 mm, c) 4.6 mm and d) 5.2 mm, between blankets 13 and 14,  $0^\circ$ - $90^\circ$  interface.*

#### 8.4.4 Failure indices progression

In the top illustrations of Fig. 8.7 the crack propagation of CT specimen is depicted through the fibre tension FI, at a four discrete crack lengths, namely 0, 3, 6 and 10 mm. The experimental study revealed that the  $0^\circ$  layer at the midsection (blanket #14) of the specimen is ultimately carrying the highest amount of load since it fails last, therefore it is shown in the FE result contours. The failed elements are presented with red colour and it can be observed that the crack is predicted to initiate in the middle of the specimen and propagates along x-axis as exactly happens in the actual test. Whilst crack propagates, the fibre compression FI at the rear end of the specimen is also illustrated in the middle row of Fig. 8.7 in order to examine whether premature compressive failure is predicted. It can be seen that failure in fibre compression appears at the crack length of 10 mm as exactly occurred during test. The compressive failure is identified in Fig. 8.4a) as the maximum load drop of the curves. Another undesired premature failure is acknowledged to be the in-plane shear, hence the evolution of the respective FI is shown in the bottom images of Fig. 8.7 for the  $90^\circ$  layer of blanket #14. A



comparison with Fig. 4.49 reveals that the areas prone to shear failure are predicted successfully by the model and validates that shear failure did not occur in any of those areas.

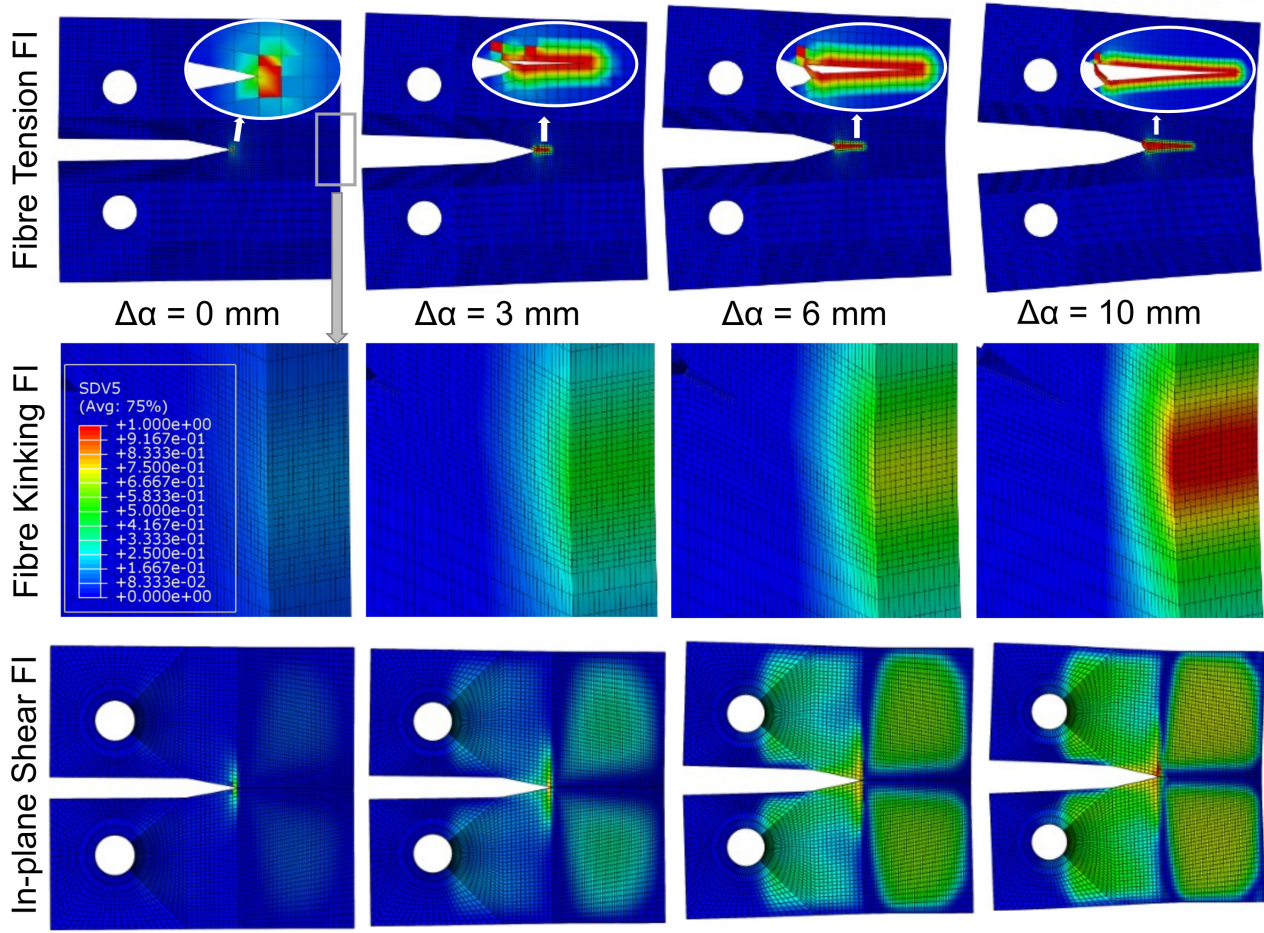


Figure 8.7: *FI at four discrete crack lengths in: (top) fibre tension at 0° layer of midsection (blanket #14), (middle) fibre compression at the rear end of the specimen and (bottom) shear failure at 90° layer of blanket #14. Failed elements are illustrated with red colour.*

#### 8.4.5 Interactive damage propagation law validation

A comparative study between a CT FEA model without interactive damage propagation enabled and one with the law enabled is employed. The damage variables for both cases are obtained from the first element at specimen's crack tip of the 90° layer of blanket #15. In Fig. 8.8, the damage variable of the interactive law ("Damage propagation interaction") is compared as a function of displacement, against the uncoupled damage variables of matrix tension,  $d_2$  (Matrix tension - No interaction) and in-plane shear,  $d_3$  (In-plane shear - No interaction). For the latter case, the evolution of the damage variables shows that these two parameters are un-

coupled and develop individually, whereas in the first case ("Damage propagation interaction") a resultant response is presented and in fact depicts the combined influence of matrix tension and in-plane shear to the ultimate failure of the integration point, resulting in an earlier failure ( $d = 1$  at 2.1 mm) than the individual damage variables  $d_2$  and  $d_3$  that reached unity at 2.8 and 3.9 mm respectively.

This is important because it predicts the non-linear response of the load - displacement curve (Fig. 8.4a)) between 1 to 3.5 mm, whereas the independent damage propagation modes correspond to a more linear behaviour of load evolution which does not correlate well with the experiments. The interactive law does not effect significantly the prediction of the maximum load since the contribution of matrix dominate failure is essentially smaller than in fibre tension which determines the CT case.

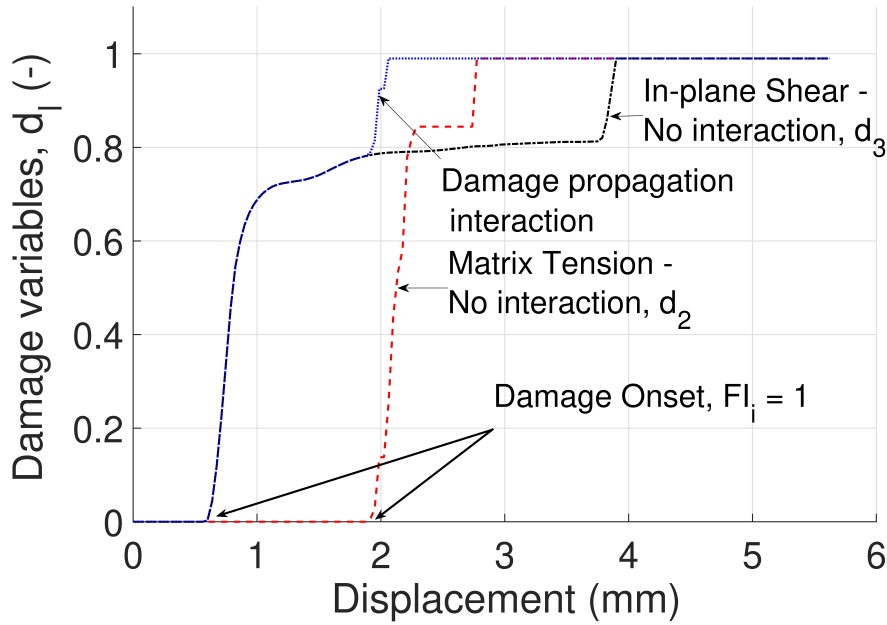


Figure 8.8: *Evolution of FI in matrix tension, in-plane shear and damage interaction law.*

Moreover, it is obvious that in any of the two scenarios the stiffness degradation due to shear occurs first at 0.65 mm, which confirms Eq. 5.51 presented in Section 5.5.3, that describes the degradation of the shear modulus. More specifically for the "No - Interaction" case, stiffness degradation due to shear occurs first, since, according to Fig. 8.8, the shear damage variable begins to obtain values greater than 0 for a displacement equal to 0.65 mm, whereas the matrix tension damage variable is initiated when the displacement reaches 2 mm. However, the final

failure is defined by matrix tension. This can be noted by the fact that the development of the "Matrix tension - No interaction" damage variable,  $d_2$  is significantly steeper leading up to unity at 2.8 mm of displacement, unlike shear damage variable, which is has a much smoother progress up to 3.9 mm where ultimate shear failure occurs.



# Chapter 9

## Low Velocity Impact - Experimental Study and Numerical Analysis

### 9.1 Introduction

Numerous studies have been carried out to examine the dynamic response of composite laminates both experimentally and numerically, though, lesser research has been performed specifically for NCF composites under LVI. Many of the relevant studies compared the mechanical behaviour and damage mechanisms of NCF composites against composites made from other fibre architectures [93, 95, 220–222] presenting very interesting results. Other researchers attempted to identify the effect of the stitches in NCF laminates under LVI loads and the developed damage mechanisms [42, 223, 224].

Nevertheless, to the best of author's knowledge, there is no study of NCF composites coupling structural imperfections with their mechanical behaviour under LVI conditions. The structural imperfections are considered as those that could possibly occur either during the manufacturing process, namely lay-up of the preform or due to structural design requirements (e.g. non-uniform blankets, ply drops). Hence, in this chapter a holistic study is presented through which NCF plates with deliberate in-plane misaligned blankets and split blankets are experimentally

studied and numerically modelled for different energy levels. Thus, the aims of the study are, firstly, to comprehend how crucial characteristics (i.e., energy absorption, mechanical response and structural integrity) of an impact plate are affected by these imperfections and secondly, to validate the constitutive model, presented in Chapter 5 against LVI conditions.

## 9.2 LVI Tests

### 9.2.1 Laminate Preparation

For the impact tests two laminates with the use of RTM method were fabricated. Whilst the exact manufacturing process was described in Chapter 3 of the thesis, in this section the formation of the stacking sequence is discussed. The first laminate was decided to incorporate deliberately misaligned blankets, whereas the second, split blankets.

For the first configuration (RTM\_1), in-plane misalignment was assigned to certain blankets in order to represent operational errors that could occur during the cut and/or the stacking process of the preform. The stacking sequence of the impact plates is  $[(-45^\circ/45^\circ) (0^\circ/90^\circ)_3 (-45^\circ/45^\circ) (0^\circ/90^\circ)_3]_S$ , however blankets 13, 14 and 15 were deliberately misaligned by an angle  $\phi_I$  as illustrated in Fig. 9.1. The reason for selecting these specific blankets to be misaligned is that they have the most significant influence in the flexural behaviour of the plate, which is the governing mechanical response during impacts of these energy levels and impactor mass [80,225]. In total three cases were studied: one without deliberate misaligned blankets ( $\phi_I=0^\circ$ ), a second with  $\phi_I = 4^\circ$  and a third with  $\phi_I = 8^\circ$ .

To achieve a consistent side by side comparison between the three examined cases, the impact plates had to be extracted from the same laminate. However, in order that to be feasible, the preform was partitioned into three equally wide segments (160 mm), each of which was characterised with the different values of  $\phi_I$  as demonstrated in Fig. 9.2.

Similarly, for the same reasons, in the second configuration (RTM\_2) it was decided to split the same blankets (Fig. 9.3). In this configuration, the split line of the blankets was attempted to

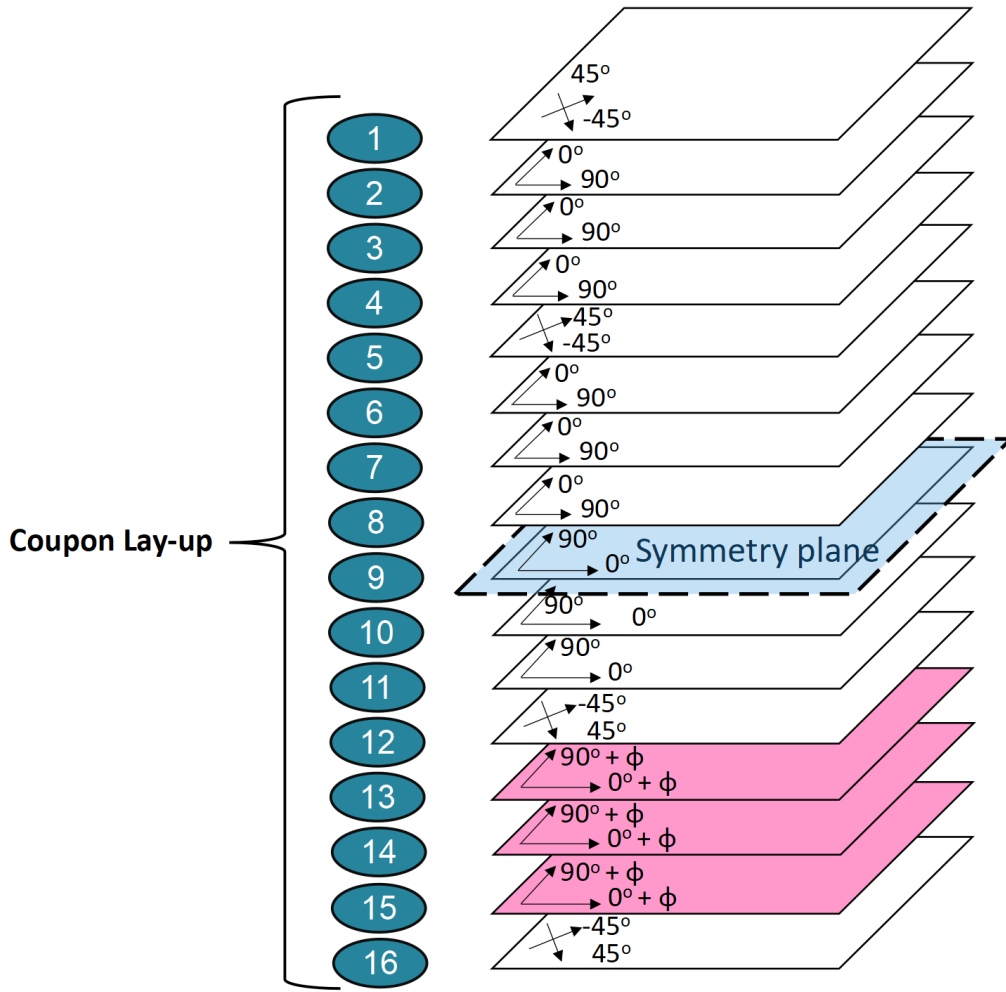


Figure 9.1: *Visualisation of lay-up sequence for an impact plate with deliberate misaligned blankets.*

cross the impact plates in the middle.

Blankets 13 and 15 were cut into three equally wide parts (i.e. 160 mm x 485 mm), whereas blanket 14 was also cut into three equal parts but perpendicularly to the other two blankets as shown in Fig. 9.4. Furthermore, in order to satisfy consistency for this configuration as well, impact plates without introduced split blankets were generated from the same laminate and tested under the same conditions (impact plates with prefix A in Fig. 9.4).

### 9.2.2 Specimen Preparation

The impact plates were water jet cut from the exact positions of the two laminates as illustrated in Figs. 9.2 and 9.4 respectively. They were later C-Scanned so that possible undesired defects

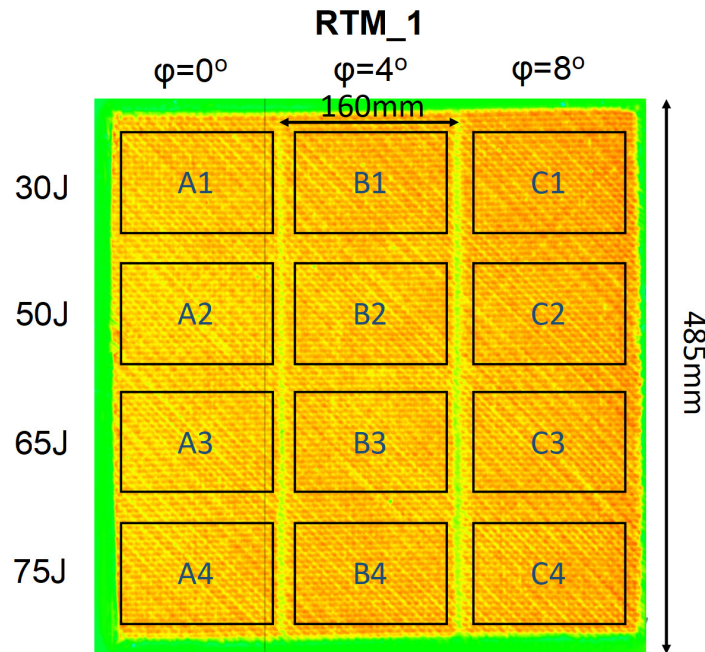


Figure 9.2: C-Scan assessment of RTM\_1 laminate with annotation of the partitioned preform.

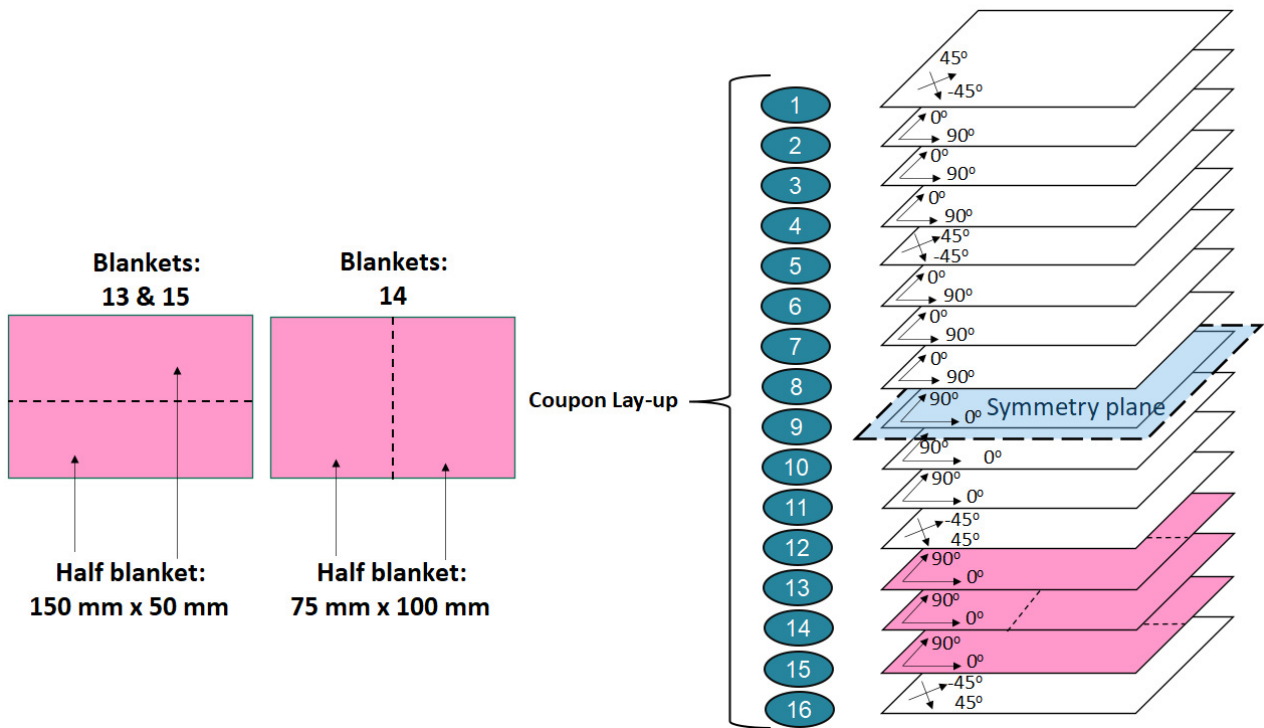


Figure 9.3: Visualisation of lay-up sequence for an impact plate with split blankets.

and flaws, generated due to the water jet cut, to be detected. In Fig. 9.5 a C-scan assessment of the fabricated plates from RTM\_1 are shown, while Fig. 9.7 presents C-scan images of the impact plates without imperfections of RTM\_2 laminate.

Regarding, the impact plates with split blankets of RTM\_2 the regions where the split blankets

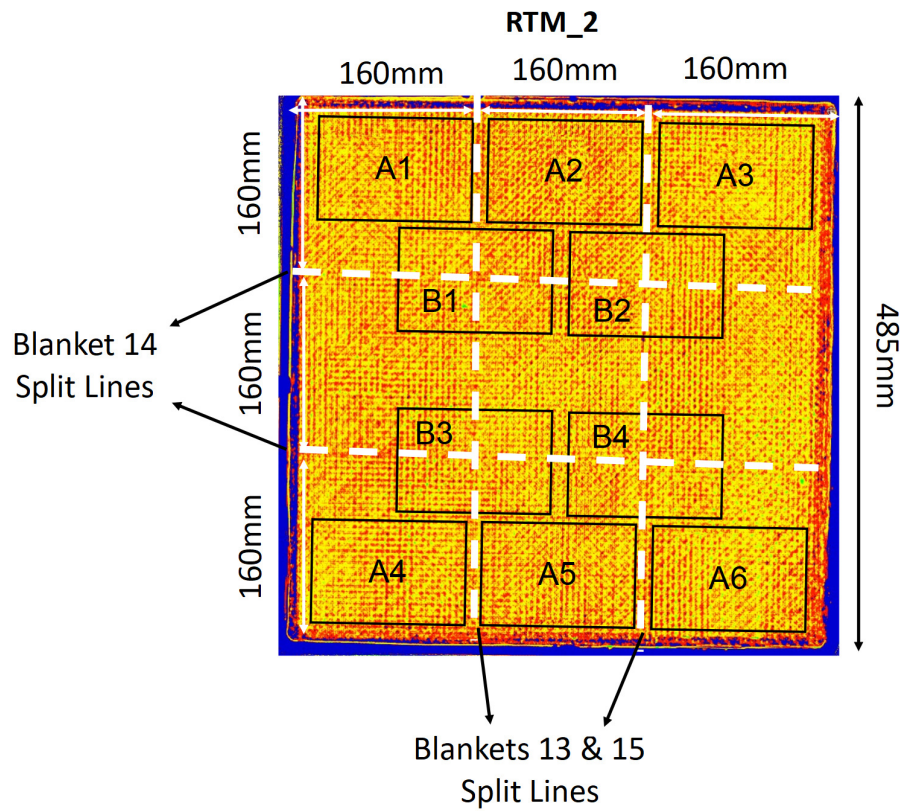


Figure 9.4: *C-Scan assessment of RTM\_2 laminate with annotation of the partitioned preform.*

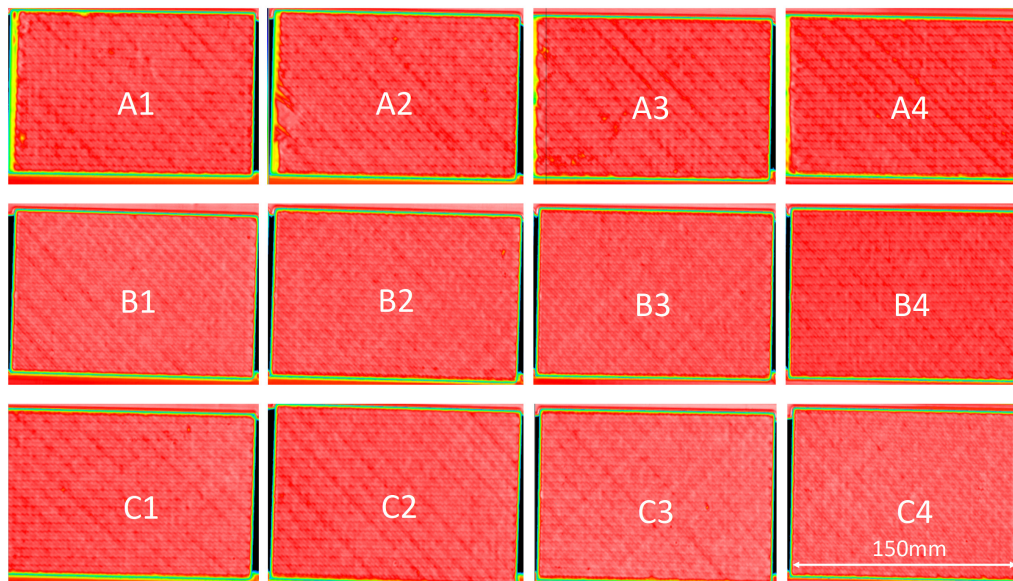


Figure 9.5: *C-Scan assessment of impact plates from RTM\_1 laminate.*

are actually in contact were also identified and presented in Fig. 9.7. The nominal dimensions of the plates are 150 mm x 100 mm x 5 mm and they are summarised for all plates in Tabs. A.12 and Tab. A.13 of Appendix A.2.



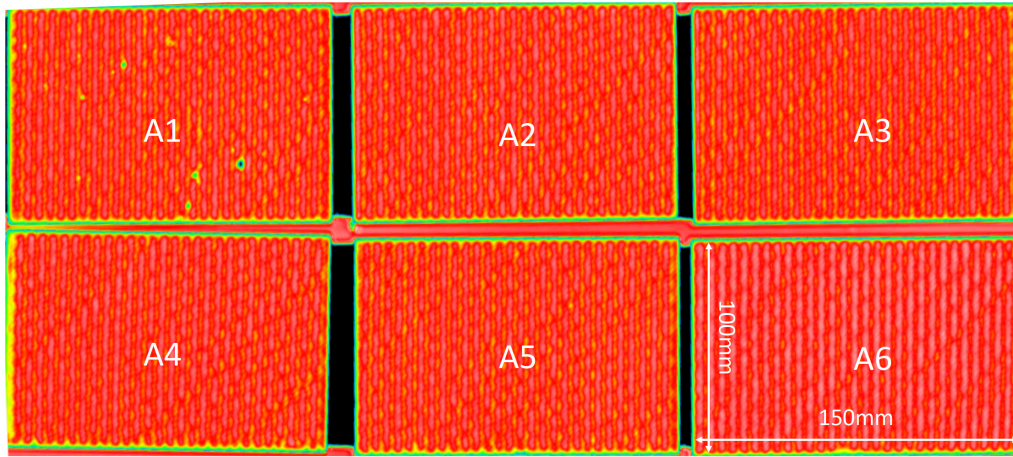


Figure 9.6: *C-Scan assessment of impact plates without imperfections obtained from RTM\_2 laminate.*

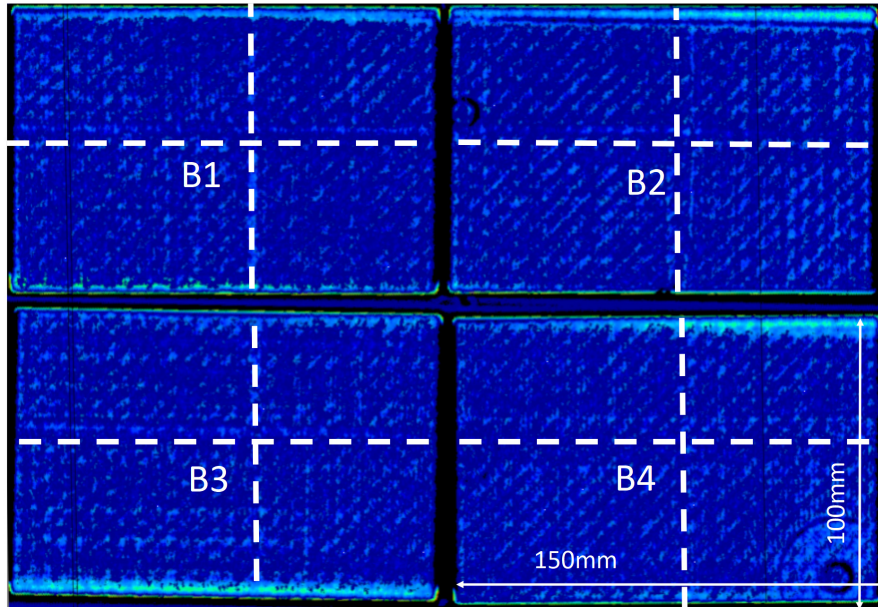


Figure 9.7: *C-Scan assessment of impact plates with split blankets from RTM\_2 laminate.*

### 9.2.3 Test Setup

The LVI tests were performed in accordance with the ASTM D7136 [143] using the Airbus test rig (Fig. 9.8 ), in which the impact plates are constrained through-the-thickness by four rubber pins and their proper positioning is defined by two pairs of guiding pins. The test rig was mounted in an Instron CEAST 9350 drop tower integrated with a calibrated 22 kN load cell which automatically provided displacement and velocity readings from the impactor tup, since both the drop height and total drop mass were given for each event.

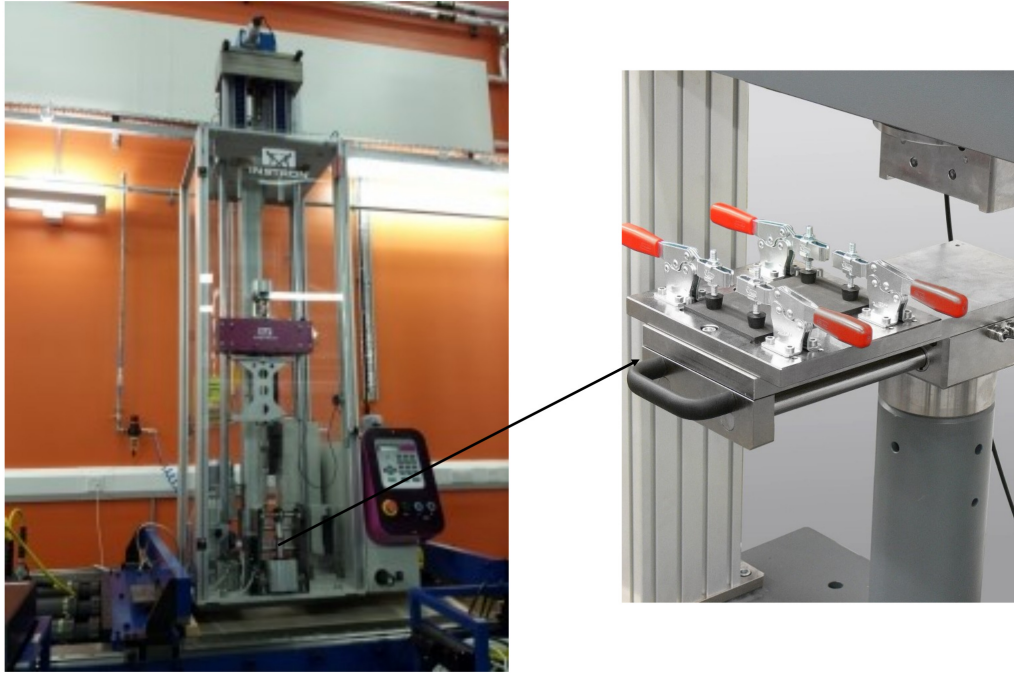


Figure 9.8: *Drop tower (left) and test fixture (right)*

The LVI tests were performed at four different energy levels (30 J, 50 J, 65 J and 75 J) so that a range of damage mechanisms, starting from interlaminar failure up to fibre breakage, to be investigated for the different plate configurations. A 2.4 kg steel hemispherical impactor of 16 mm diameter was used for the impact test, however three supplementary mass packers were used resulting in a total mass of 5.392 kg. The impact events were monitored by using a Phantom v25.11 high speed camera at  $10^5$  frames per second, which recorded the footage of the bottom face of each panel through a mirror positioned underneath the impact panel. Also, two Dedocool DC anti-flicker lights targeted at the panel face in order to generate the correct ambient lighting.

## 9.2.4 Case A - Misaligned NCF Blankets

### 9.2.4.1 Test Results

The test results of the misaligned impact plates are summarised in the current subsection. One impact plate per energy level per degree of misalignment was tested. For the sake of clarity, "A" regards impact plates with  $\phi_I = 0^\circ$ , "B" with  $\phi_I = 4^\circ$  and "C" with  $\phi_I = 8^\circ$ . Fig. 9.9 presents the force-time curves for all impact plates at all energy levels. All curves

reveal, for the energy levels of 30 and 50 J, similar behaviour. In configuration "A" the peak load is somewhat lower and the duration of the event is marginally longer ( $4.03 \mu\text{s}$ , while "A" and "B" app.  $3.83 \mu\text{s}$ ), while some stronger oscillations are observed which correspond to the primary failure mode which is matrix tensile failure. At 65 J, unlike configurations "B" and "C", configuration "A" exhibits a sudden load drop which is associated to fibre failure. Then in 75 J, configuration "B" is characterised by the same load drop, whereas for configuration "C" that is not the case. Hence, it appears that as blanket misalignment is increased, fibre breakage is postponed. This can be explained by the fact that due to this misalignment a stress component is generated resulting in intrabundle matrix failures during bending. These matrix dominated failures although are not the dominant failure modes in terms of energy absorption, they absorb an amount of energy which is not channelled to fibre breakage.

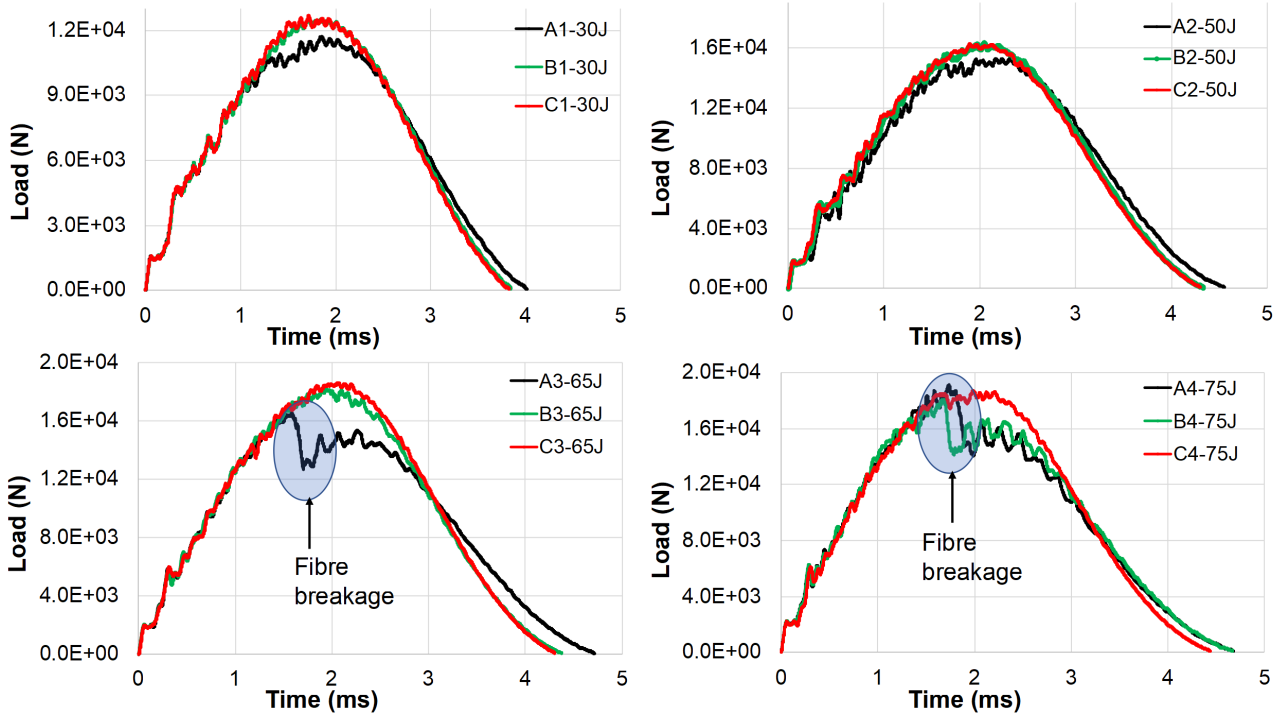


Figure 9.9: Load - Time curves of the impact plates with deliberately misaligned blankets ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ) for 30 J, 50 J, 65 J and 75 J.

The load-displacement curves are shown in Fig. 9.10. As expected, the displacement soars as impact energy increases, while the enclosed area by the curves indicates the absorbed energy by the laminate. At all energy levels the initial slope for all configurations remains constant at least until peak load, meaning that there is no significant difference in laminate stiffness.



However, after peak load a change of the slope during unloading is observed in configuration "A" at all impact energy levels and configuration "B" at 75 J which indicate nominal stiffness reduction owing to the various damage modes. Also, it worth commenting that the maximum displacement does not occur at peak load but when the velocity of the impactor turns to zero, which drives the impactor to rebound.

At lower and moderate impact energy levels (30 J and 50 J), some fluctuation is noted during loading particularly for "A" plate which is due to the indentation and matrix crack generation. Though, at 75 J for "A" and "B", some fluctuation appears after the peak point with longer unloading phase. Some fibre damage can be assumed which is also validated by the load drops. Indeed, as reported in other studies [220], in view of these comparisons, the bending stiffness, and in principle, the in-plane properties could differ due to the difference in the stacking sequence (because of the deliberate misalignment).

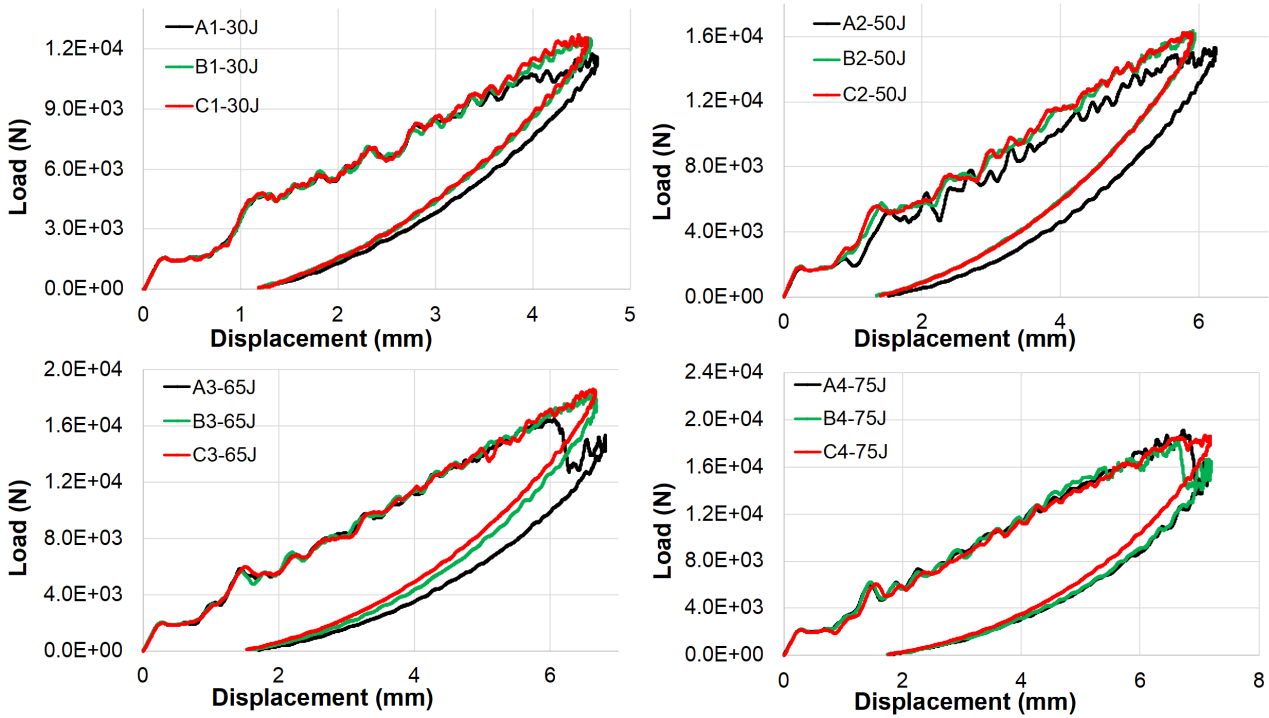


Figure 9.10: Load - Displacement curves of the impact plates with deliberately misaligned blankets ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ) for 30 J, 50 J, 65 J and 75 J.

The impact energy-time curves of the three configurations for the four energy levels are presented in Fig. 9.11. During impact, the kinetic energy of the impactor is gradually transferred to the plate as soon as contact is made. The energy is initially absorbed as elastic deformation,

vibration of the laminate and ultimately as damage formation. The elastic energy is responsible for the rebound of the impactor, whereas the vibration of the laminate is fair to be considered negligible [226]. Although, the absorbed energy can be calculated from the enclosed area of the load-displacement curves, as previously stated, in this work it was opted to be graphically obtained as shown in Fig. 9.11. It is obvious that the absorbed amount of energy is higher for plate "A" when compared against plates "B" and "C", which is particularly justified by the fibre failure at 50 J and 65 J. Tab. 9.1 summarises the ratio between absorbed energy and impact energy for all configurations at all energy levels. At the lowest level of impact (30 J) the ratio of "A" was higher but still comparable with configurations "B" and "C". This ratio is increased significantly as impact energy level increases and more energy demanding damages occur, namely fibre fracture.

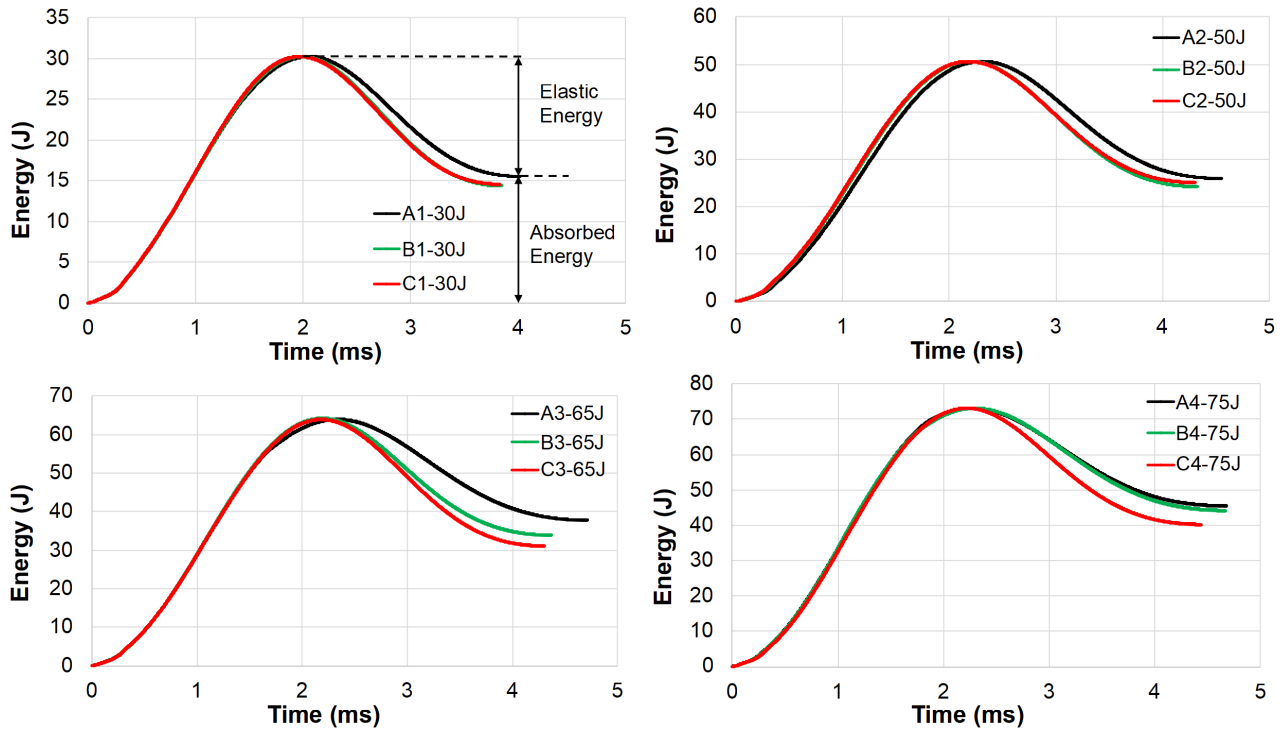


Figure 9.11: *Energy - Time curves of the impact plates with deliberately misaligned blankets ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ) for 30 J, 50 J, 65 J and 75 J.*

Figs. 9.12 (left and right) summarise the peak loads and the absorbed energies for each configuration at each energy level. It is noteworthy that the peak load is constantly lower for configuration "A" at all energy levels and increases as energy level becomes higher. Moreover, configuration "C" begins to exhibit noticeable higher peak load than configuration "B" from 65

| Absorbed to Impact Energy Ratio |        |        |        |
|---------------------------------|--------|--------|--------|
| Energy Level \ Plates           | A      | B      | C      |
| 30J                             | 51.73% | 47.87% | 48.53% |
| 50J                             | 51.80% | 48.42% | 49.08% |
| 65J                             | 57.94% | 51.95% | 50.58% |
| 75J                             | 60.41% | 58.71% | 56.44% |

Table 9.1: *Percentage of energy absorption in relation to the impact energy per plate configuration ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ) for 30 J, 50 J, 65 J and 75 J.*

J and on, while beyond 50 J the slope of peak load tends to plateau, which implies that plates release the impact energy through fracture processes. Concerning energy absorption, at all impact energy levels, the misaligned plates dissipated lower energy compared to non-misaligned ("A"). This phenomenon is more apparent at 65 J and 75 J meaning that configuration "A" absorbs higher amount of energy. Hence it can be concluded that when there are fibre misalignments within a stacking sequence, an amount of energy is smeared in different matrix dominated failures which do not increase significantly the energy absorption capability of the plate, but they to exhibit improved damage resistance and increased carrying load capability.

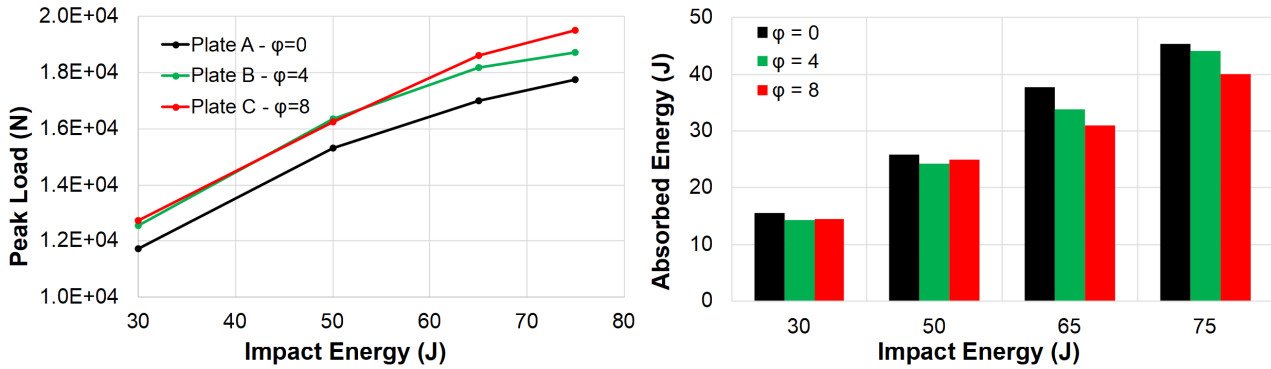


Figure 9.12: *Summarised test results of Peak Load (left) and Absorbed Energy (right) of the impact plates with deliberately misaligned blankets ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ) in function of impact energy level.*

#### 9.2.4.2 Fractography

After the impact tests, the damaged plates were post-processed by visual inspection and ultrasonic C-Scan in order to enable a comprehensive investigation of the damage on each configuration. In Fig. 9.13 the projected delaminated area at the interface between blankets 12 and 13 (i.e.  $45^\circ/(90^\circ+\phi_I)$  interface) of all laminates at all the impact energies obtained from the C-scan inspection is shown. At first sight it can be noted that delamination is a significant failure mode at

all energy levels and for all configurations. Also, it appears that the plate configuration without deliberate fibre misalignment  $\phi_I = 0^\circ$  exhibits increased projected delaminated area when compared to the other two configurations. There is not an obvious reason for this behaviour, although in other studies it has been reported that delamination is inversely proportional to the difference of fibre angle between adjacent blankets [62]. One reason could be that in the misaligned configurations, intrabundle matrix tensile failure occurs during flexure deformation, which absorbs a significant amount of energy and prevents from extensive delamination.

With regards to the shape of the projected delaminated area, at 30 J an irregular circular shape is formed for all configurations which is then developed to an irregular ellipse for 50J impact energy. For configuration "A", at 65 J and 75 J the projected shape changes further to rectangular with certain offset from the symmetry plane, whilst for 65 J, configurations "B" and "C" exhibit an irregular rhomboid shape. The rectangular shape of delamination is also formed for configuration "B" at 75 J, whereas configuration "C" maintains the rhomboid shape.

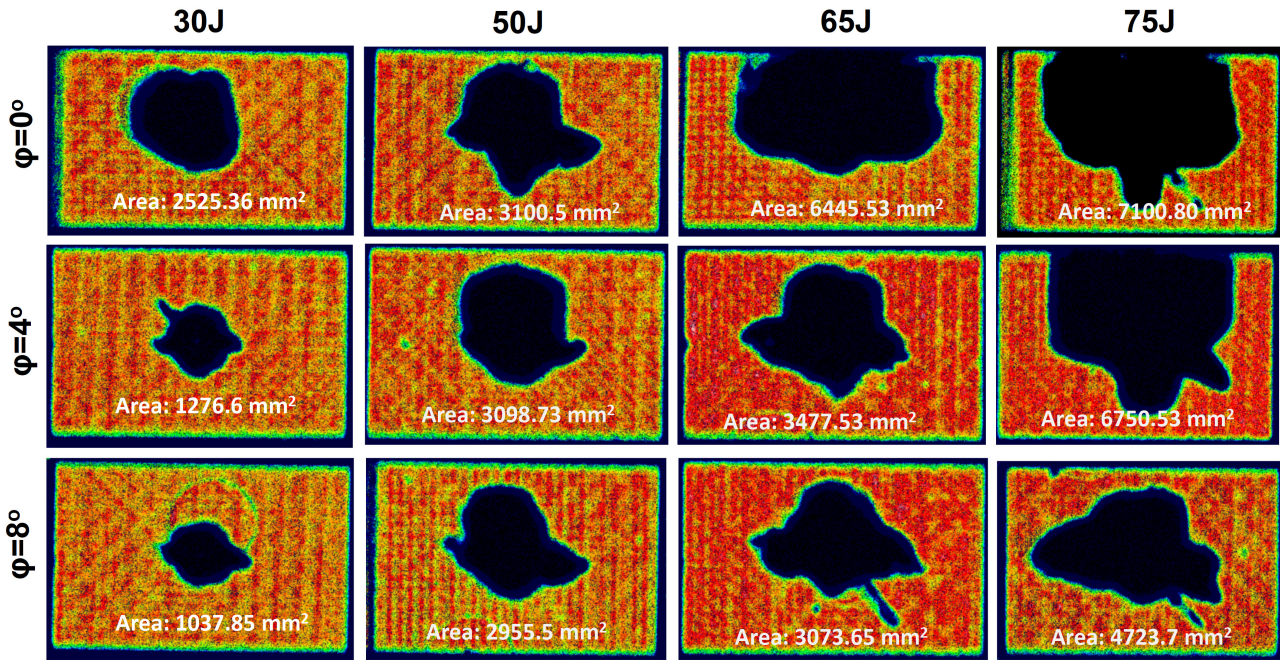


Figure 9.13: *Projected delaminated area at the interface between blankets 12 and 13 for all laminates at all energy levels ( $A=0^\circ$ ,  $B=4^\circ$ ,  $C=8^\circ$ ).*

Fig. 9.14 displays the photos of the bottom surface of the impacted specimens of all configurations at all energy levels. At 30 J all three configurations present very little or even negligible impact damage, without much of visual difference in dent depth. Transverse failure of bottom



blanket appears at 50 J of impact energy level, which is expressed as limited fibre splitting along the  $45^\circ$  layer. At 65 J though, different observations are made; in configuration "A", in addition to matrix failure, fibre breakage is visible, whereas for configurations "B" and "C", fibre splitting and extensive matrix cracks were noted. Fibre breakage appears to be the main failure mode for configurations "A" and "B" at the back face of the plate, while the main damage of the bottom surface for configuration "C" is still extensive fibre splitting and matrix cracking.

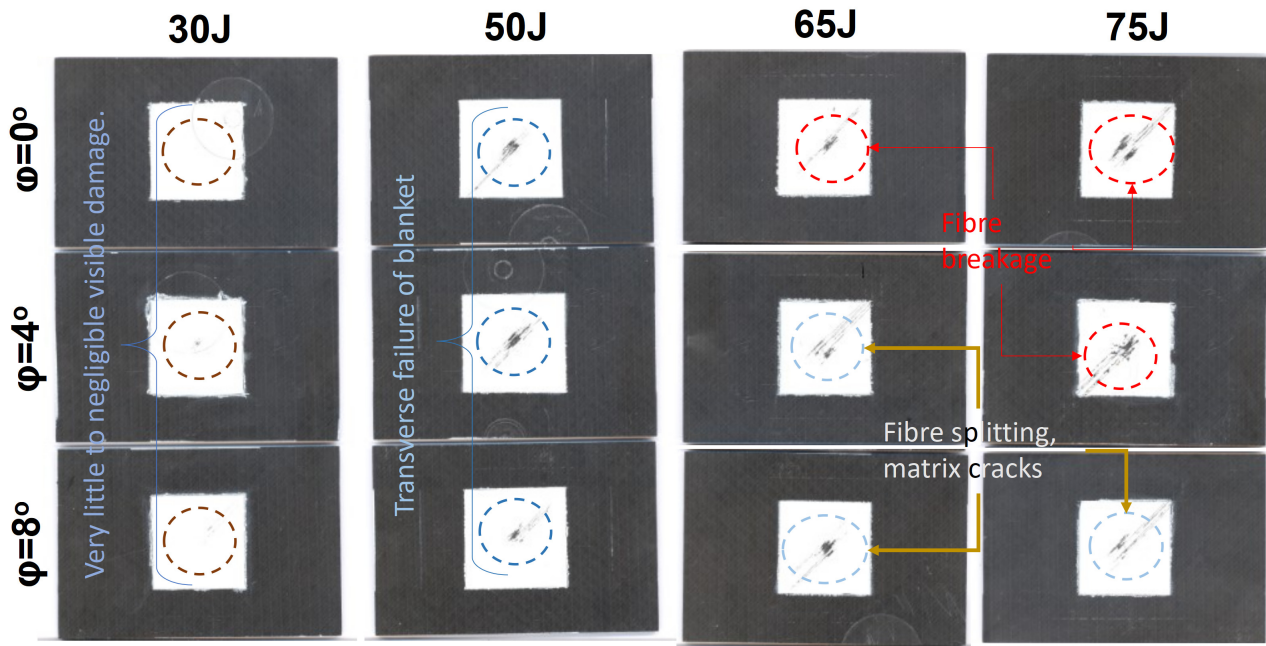


Figure 9.14: Photos of the impacted bottom faces of all plates at all energy levels.

## 9.2.5 Case B - Split NCF Blankets

### 9.2.5.1 Test Results

The comparison of the experimental results between the impact panels with uniform blankets against those with split. As for the previous case, one plate per energy level per configuration was tested. The force-time curve for both configurations at all energy levels are shown in Fig. 9.15. For low energy levels (30 J and 50 J) the curves for both configurations are symmetric with sinusoidal shape, same peak load and duration. The uniform blanket configuration exhibits less smooth loading than the split blanket configuration which denotes the first stages of delamination. At higher impact energies (65 J and 75 J), the mechanical response of the

split blanket configuration is described by a sudden load drop and longer duration (4.4  $\mu$ s and 4.3  $\mu$ s) which can be associated both with fibre breakage and extended matrix failure at the interface between split blankets, while the uniform blanket configuration maintains a smooth trend with only limited oscillations.

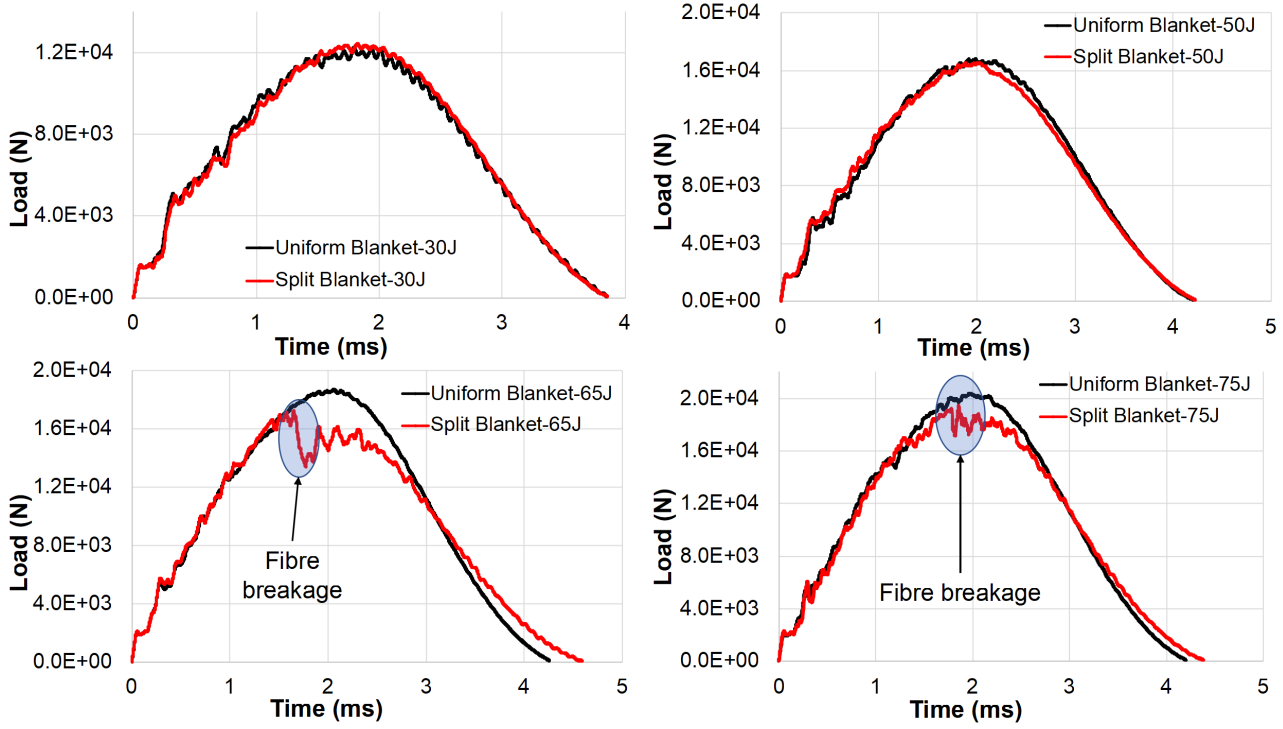


Figure 9.15: Load - Time curves of uniform and split blanket impact plates for 30 J, 50 J, 65 J and 75 J.

With regards to the load-displacement curves, which are shown in Fig. 9.16, there is some small deviation between the two configurations in the slope of the curve during loading phase. Specifically, the split blanket configuration appears to have slightly a more gentle slope which implies that the bending stiffness of these plates is reduced. That could be explained by the interrupted fibres of blankets 13, 14 and 15 which are crucial to carry flexural loads. The oscillations of the split blankets configuration are more severe in particular for 50 J, 65 J and 75 J, which indicate the excessive matrix fracture and delamination, followed up by fibre breakage for the 65 J and 75 J. Interestingly, there is no difference in unloading curves, which shows that there is energy absorption through local damage.

Fig. 9.17 clearly reveals that although at 30 J energy absorption is almost identical for both configurations, the difference becomes more apparent in favour of the split blanket configuration

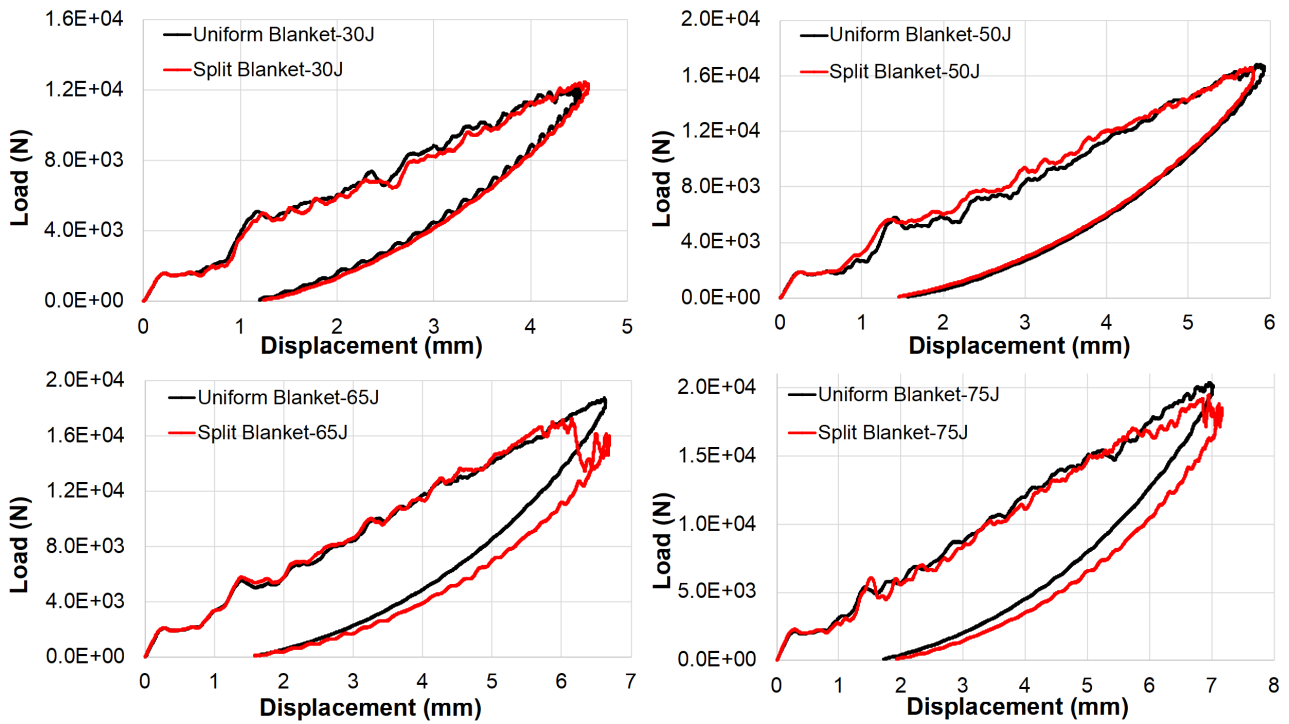


Figure 9.16: Load - Displacement curves of uniform and split blanket impact plates for 30 J, 50 J, 65 J and 75 J.

as the energy level increases. This behaviour is explained by the extensive failure of the generated resin rich area at the interface of the blanket which absorbs an essential amount of energy. The ratios of energy absorption to impact energy for both configurations are summarised in Tab. 9.2. It is impressive that the difference in energy absorption ratios increases almost exponentially (from 0% to 11%) with the increased impact energy, revealing the clear superiority of split blanket configuration in absorbing higher amount of energy. On the other hand, uniform blanket configuration is characterised by an improved damage resistance capability and overall structural integrity.

| Absorbed to Impact Energy Ratio |                 |               |
|---------------------------------|-----------------|---------------|
| Energy Level \ Plates           | Uniform Blanket | Split Blanket |
| 30J                             | 50.80%          | 50.07%        |
| 50J                             | 49.32%          | 51.68%        |
| 65J                             | 48.69%          | 57.18%        |
| 75J                             | 48.28%          | 59.28%        |

Table 9.2: Percentage of energy absorption in relation to the impact energy of uniform and split blanket configurations for 30 J, 50 J, 65 J and 75 J.

Figs. 9.18 (left and right) summarise the peak loads and the absorbed energies for both con-

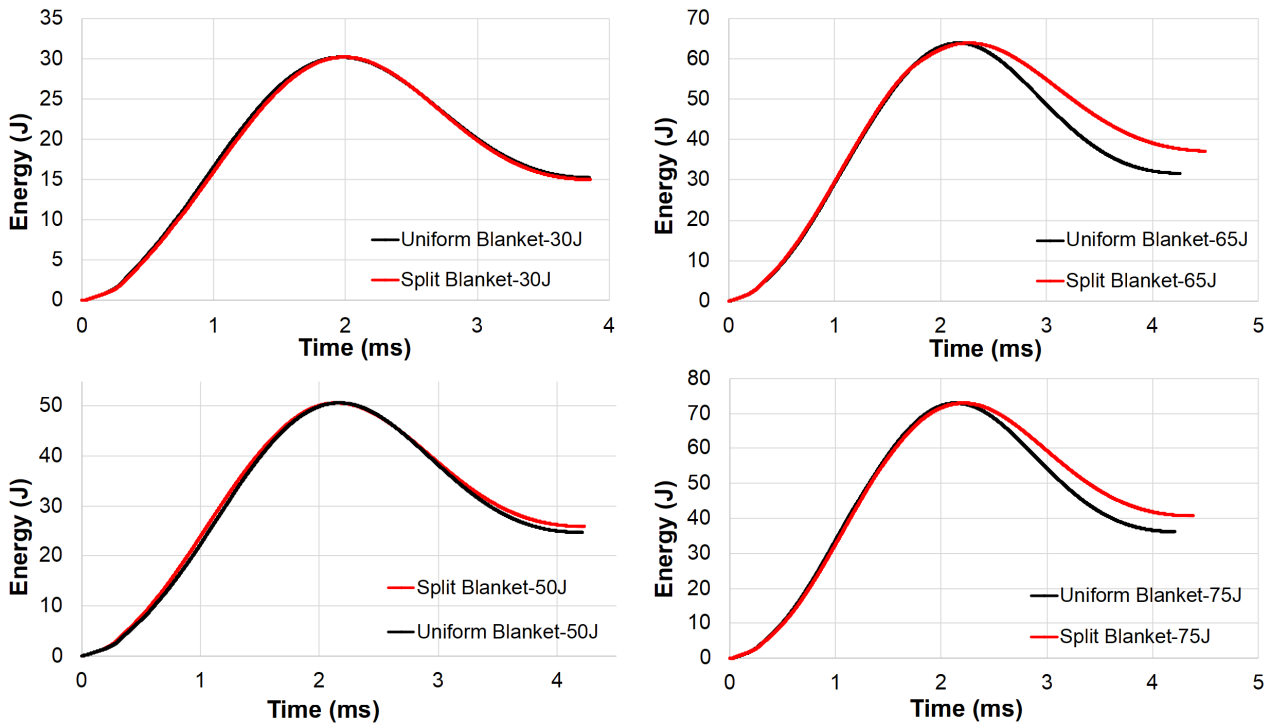


Figure 9.17: *Energy - Time curves of uniform and split blanket impact plates for 30 J, 50 J, 65 J and 75 J.*

figurations at each energy level. As stated previously, the peak load increases with impact energy but until 50 J there is only marginal difference between the two configurations. From 65 J onwards, uniform blankets exhibit the highest peak load, while split blanket configuration show a decreasing trend with higher impact energy. This behaviour is due to the extensive matrix damage and fibre damage which causes the split blanket plates to be more brittle and less flexible than their counterpart. The observations above are also translated in the absorbed energy-impact energy chart, in which at 30 J and 50 J of impact energy, both configurations dissipated almost the same amount of energy, however when impact energy increases, the uniform blanket configuration is able to maintain the structural integrity.

#### 9.2.5.2 Fractography

The same fractography procedure was followed as for Case A. The C-scans of the uniform and split blanket configurations are shown in Fig. 9.19. The images are obtained at the interface of blankets 12 and 13, namely uniform to split blanket interface. The uniform blanket configuration exhibited clearly increased projected delaminated area than the split blanket



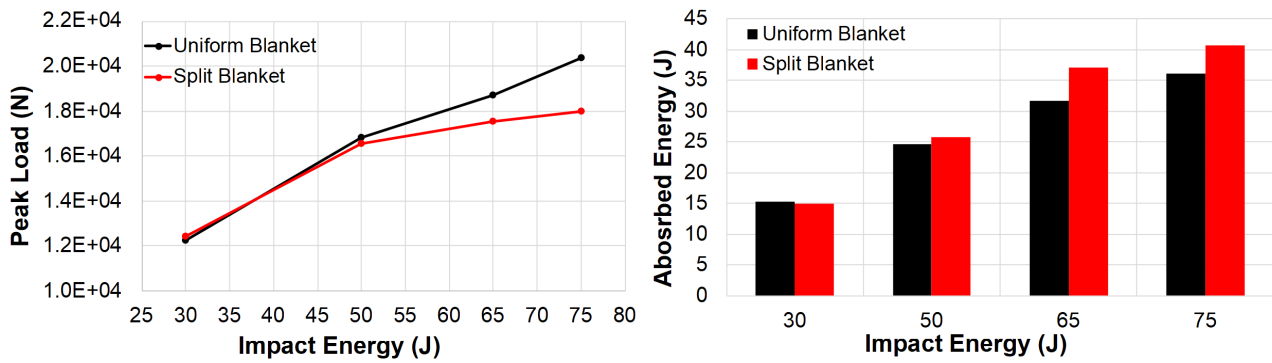


Figure 9.18: Summarised test results of Peak Load (left) and Absorbed Energy (right) of the impact plates with uniform and split blankets in function of impact energy level.

configuration even up to 32% (for 75 J), which is though explained by the fact that the interface between the split halves of the blankets fails too and absorb an important amount of energy which is not channelled to the delamination of the adjacent blankets. Again, the shape of the projected delaminated area changes as the impact energy increases and other damage mechanisms are developed. The irregular circular shape of the 30 J energy level is gradually transformed to ellipsoid (50 J) and then to a hybrid shape between ellipse and rectangle (65 J and 75 J).

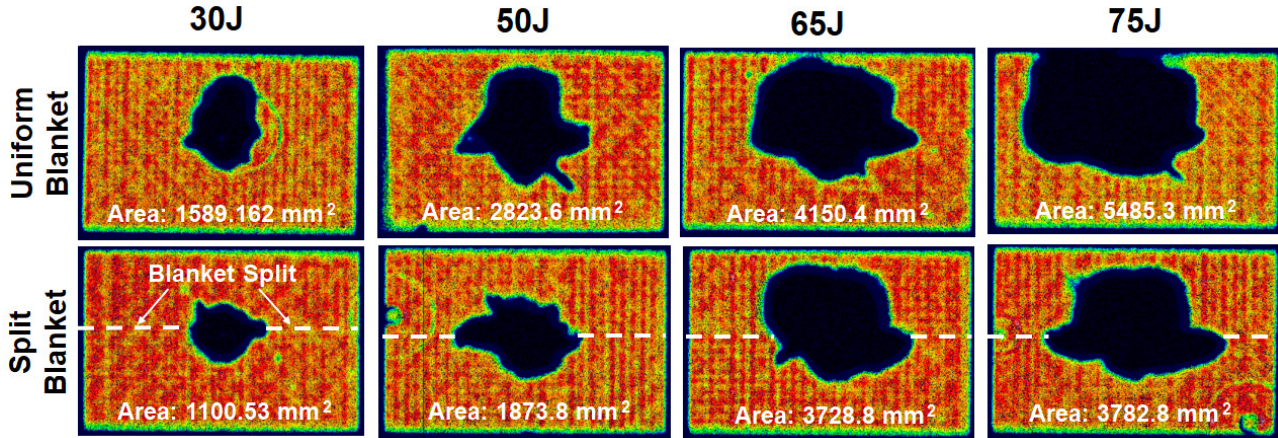


Figure 9.19: Projected delaminated area at the interface between blankets 12 and 13 for split and uniform blanket configurations at all energy levels.

The photos of the bottom surface of the impacted specimens for both configurations at all energy levels is presented in Fig. 9.20. Barely visible impact damage is observed in both configuration at 30 J of impact energy level, followed by matrix dominated transverse failure of the bottom layer at 50 J. The back face of the uniform blanket configuration was characterised by limited fibre splitting and severe matrix cracks for 65 J and 75 J, whilst for the same energy

levels the bottom layer of the split blanket configuration exhibits fibre breakage.

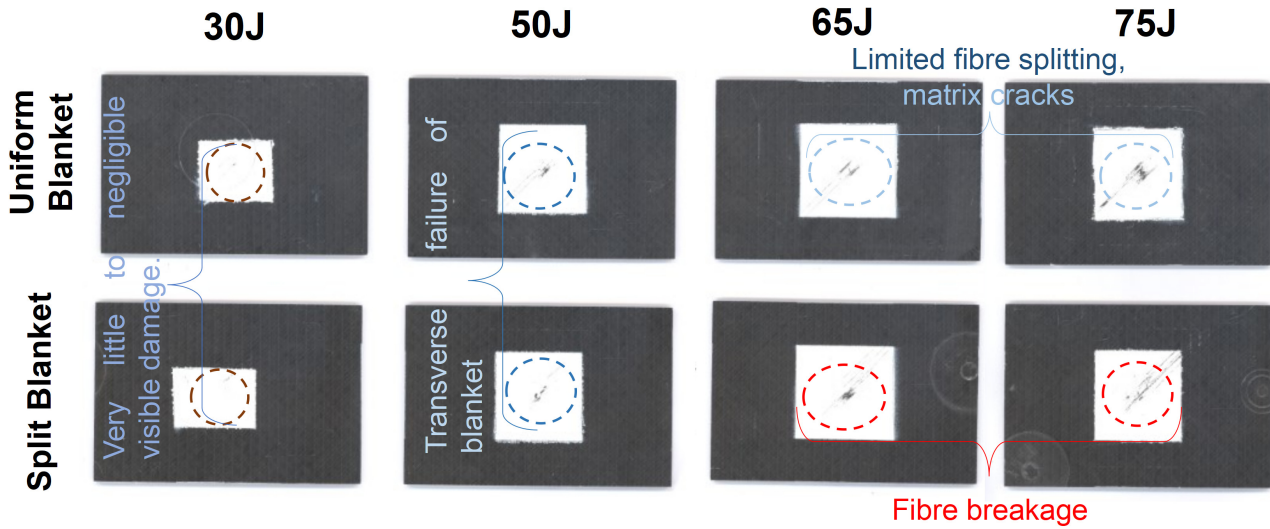


Figure 9.20: Photos of the impacted bottom faces of plates for uniform and split blanket configurations at all energy levels.

### 9.3 Numerical Analysis of LVI cases

The mechanical response of the LVI plates for different energy levels as presented in the previous section of this Chapter, is modelled with the assistance of FE methods. As for the analyses of Chapters 7 and 8, the analyses of the current Chapter are carried out in Abaqus/Explicit implementing the constitutive model presented in Chapter 5 through a VUMAT subroutine. Although lay-up symmetry typically exists in y-z and x-z planes, a full modelling approach was preferred, instead of symmetry models, since the imperfections introduced in the impact plates can result in a non-symmetric mechanical behaviour. Its prediction capabilities are assessed in terms of load response and energy absorption, while the predicted damage and failure patterns are compared against the experimental findings for both cases at all energy levels.

#### 9.3.1 Model Set-up and Boundary Conditions

The LVI tests were attempted to be modelled as close to the real conditions as possible. The impact plate was modelled with stacked continuum shell elements with reduced integration (SC8R) in order to alleviate possible shear locking phenomena during bending [227]. Each

layer of an NCF blanket is described by 3 integration points while the interaction between the blankets was represented with the cohesive surface approach (Fig. 9.21) as also performed by Hongkarnjanakul et al. [228] with excellent results. Enhanced hourglass control was enabled in order to minimise the effect of the reduced integration of the continuum shell elements, which leads them to be excessively flexible, and thus inconsistent in bending deformations. The impact plates are partitioned according to the areas of the plates which exhibited similar in- and out-of-plane fibre misalignment as recorded by optical microscope. The fibre misalignment angles per blanket for each LVI case are summarised in Tabs. B.27 and B.28 of Appendix B.2.5 for Case A and Tabs. B.29 and B.30 of Appendix B.2.6 for Case B.

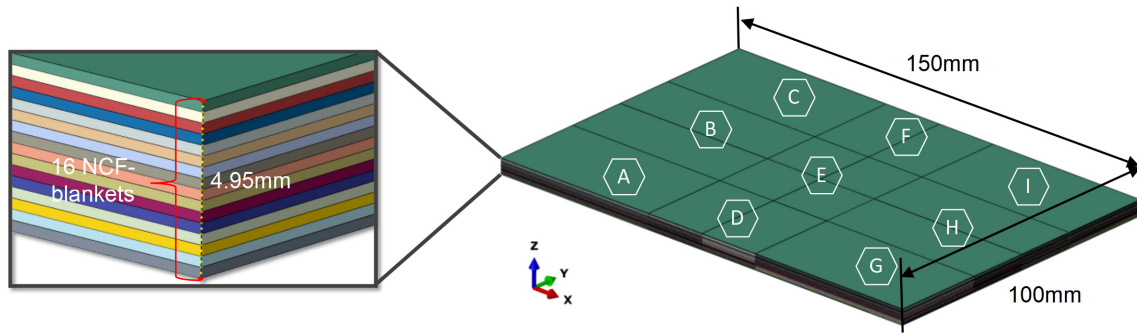


Figure 9.21: *LVI plate dimensions and partitions assigned different values of in- and out-of-plane fibre misalignment.*

The test rig was modelled with 3D solid elements (C3D8), with global mesh size of 3 mm (Fig. 9.22) using steel properties ( $E = 200$  GPa, density =  $8050 \text{ kg/m}^3$ ) [229], while the impactor was of hemispherical shape of 16 mm diameter and 15 mm height, and it was modelled with rigid shell elements (R3D4), assigned a global mesh of 0.5 mm and a point mass inertia of 5.392 kg (as calculated by Instron data logger from the summation of the impactor components) (Fig. 9.23). The toggle clamps were modelled at cubic shape of 10 mm x 10 mm x 5 mm, as rigid bodies.

With regards to the boundary conditions, the test rig was fully clamped at all edges, the impactor was set in the middle of the plate, constrained in five degrees of freedom except from the vertical translation (z-axis,  $U_z \neq 0$ ) in which a predefined initial velocity was applied: - 3.696 m/s for 30 J, -4.307 m/s for 50 J, -4.91 m/s for 65 J and -5.274 m/s for 75 J. The impact plate is constrained at one vertical side (100 mm long) in x- and at one horizontal side (150

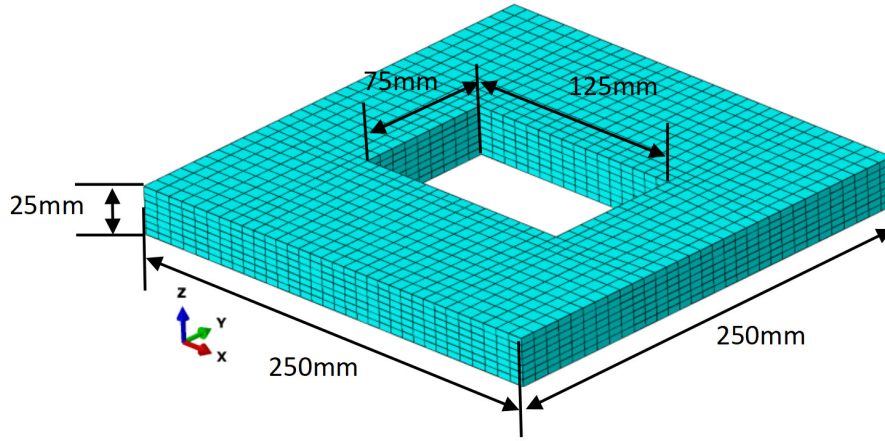


Figure 9.22: Illustration of the base plate FE model.

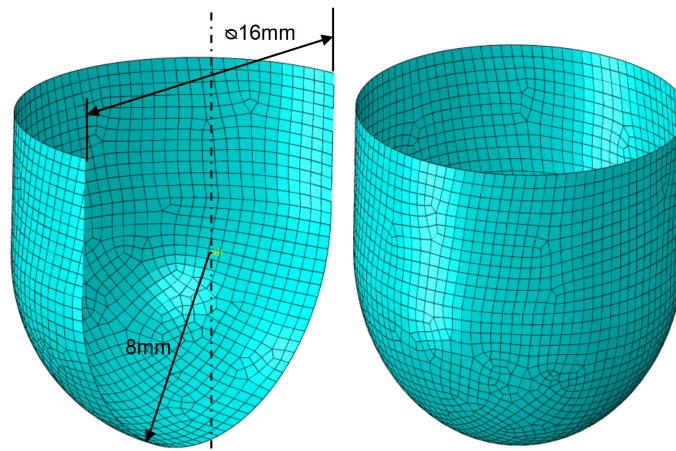


Figure 9.23: Illustration of the impactor FE model.

mm long) y-direction ( $U_x = 0$ ,  $U_y = 0$ ) as a result of the restrain pins. Moreover, the toggle clamps are constrained both in the xy plane, and in the vertical axis (z). Fig. 9.24 illustrates the boundary conditions of the involved parts for the numerical modelling of the LVI cases. A gravity load of  $-9.81 \text{ m/s}$  in the z-direction was applied throughout the entire model step for all elements.

The numerical results are directly compared against the experimental testing by plotting the relative displacement, velocity, and load with respect to time, obtained from the centre of mass of the impactor. The load was calculated by multiplying the mass of the impactor with the reported acceleration of the impactor.

The interaction between the parts of the LVI is presented in Fig. 9.25, and is characterised



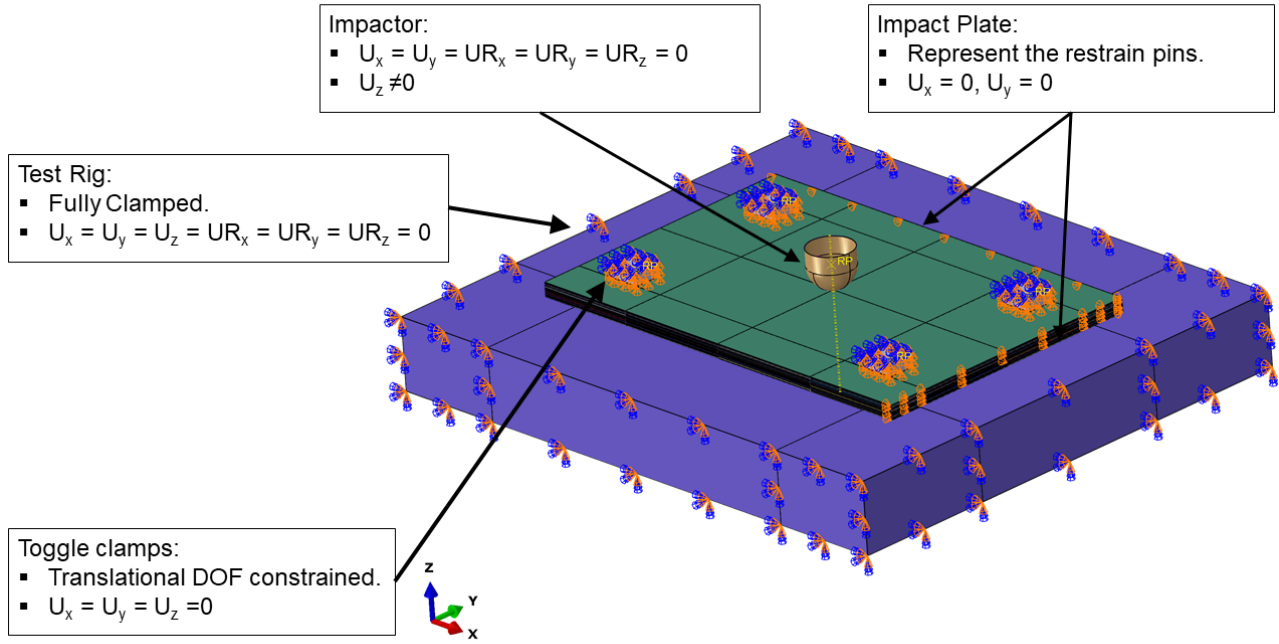


Figure 9.24: Schematic illustration of the boundary conditions of the LVI FE model.

by a "hard-contact" with global tangential friction coefficient of 0.3. The cohesive properties between the blankets of the impact plate are described by the cohesive law and the numerical data presented in Chapter 6 as obtained from the FE analysis of Mode I and Mode II specimens. However, due to the size of the impact plate and the effective "gauge length" in which extensive delamination can develop, the corresponding cohesive properties for steady-state conditions were selected. Furthermore, in order to alleviate the possibility the nodes of one blanket penetrating to the other during impact, the top blanket of each cohesive couple was defined as "Master", whereas the bottom as "Slave", according to Bogenfeld et al. [230] recommendation. Similarly, the same relation was defined between the impactor and the impact plate, where the impactor was assigned as "Master" part and the impact plate as "Slave".

For the specific LVI case of the impact panels with split blankets, the contact area between the two halves of blankets 13, 14 and 15 is modelled with the cohesive surfaces approach, and it is assigned the matrix properties of the SD8100/SR8824 resin system as presented in Tab. B.3 of Appendix B.1.1, since the interface between the halves of the blankets are resin rich areas. Moreover, concerning the translaminar fracture toughness, for all LVI FE models, the steady state value,  $G_{ICT_{Prop.}}$  was introduced since the length of the cracks produced during

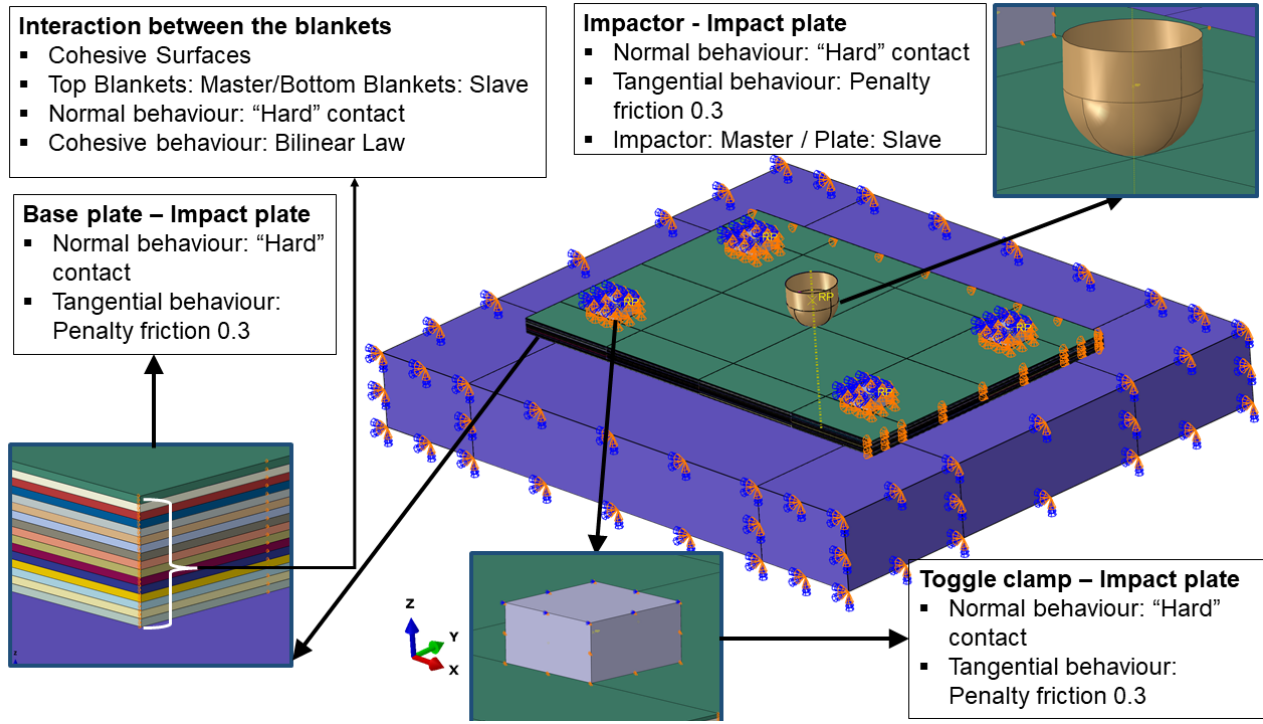


Figure 9.25: Schematic illustration of the interactions between the involved parts of the LVI FE model.

impact are essentially longer than the initial crack region found within the R-curves and thus they are characterised by the translaminal fracture toughness of propagation. Additionally, the constitutive model requires a single critical energy release rate, hence the  $G_{ICT_{Prop.}}$  values were used.

### 9.3.2 Analysis Parameters

In the original version of the constitutive model [12] a permanent strain constant is introduced as a mean to describe plastic strain of an element upon damage onset. The total plastic strain as a function of the permanent strain constant,  $\beta$  is defined by Iannucci and Ankersen [12] in an fully analytical formulation according to the following relationship:

$$\Delta \epsilon_{pl} = \left[ 1 - \frac{(1 - A) \cdot \epsilon + A \dot{\alpha} \epsilon_{0,i} (1 - \beta)}{(1 - d) \epsilon} - \frac{\sigma A \dot{\alpha} \epsilon}{E_0 (1 - d)^2 \epsilon^2} \right] \Delta \epsilon \quad (9.1)$$

where  $\epsilon_{pl}$  is total plastic strain,  $E_0$  is the undamaged Young's modulus and  $A = \frac{\epsilon_{max,i}}{\epsilon_{max,i} - \epsilon_{0,i}}$ , and  $\dot{\alpha}$  is the strain rate enhancement factor. Also, implementing permanent strain constant at

Hooke's Law, the form is:

$$\Delta\sigma = E_0 \cdot (1 - d)\Delta\epsilon - \beta \frac{d\sigma}{1 - d} \quad (9.2)$$

where, the material constant,  $\beta$  determines the magnitude of the permanent damage strain. Hence, an in-plane analysis to a continuum shell element is performed, containing the material properties of RTM\_1 (Tab. B.7 in Appendix B.1.4) in order to verify the suitability of this modelling approach. The element was set to 1 mm x 1 mm x 0.25 mm dimensions in order to satisfy the dimensional restrictions that exist for the continuum shell elements between the in-plane and the through-the-thickness dimensions. The boundary conditions were allocated such not to restrict Poisson's ratio effect and a displacement rate of 1 mm/s for loading and unloading was applied, which also ensured complete failure of the element. Three discrete values of  $\beta$  were examined namely, 1, 10 and 20.

For  $\beta = 1$  the stress-strain curve returns back to the origin as shown in Fig. 9.26, implying that there is no permanent damage, whereas for values of  $\beta$  greater than unity irreversible strain is noticeable. Specifically, for  $\beta = 10$  after failure onset (peak load), damage will propagate resulting in degradation of the stiffness. When damage index,  $d$  is equal to 0.5 and prior to the ultimate failure the element is unloaded, a positive permanent strain is noted. For  $d = 0.75$ , as expected, the slope of the unloading curve is steeper, corresponding to increased degradation of stiffness but ending to the same value of permanent strain. Similarly, for  $\beta = 20$  and failure indices of 0.5 and 0.75, the irreversible strain is larger than  $\beta = 10$ .

The final value of term  $\beta$  for the LVI analyses is determined after performing a series FE simulations, to accommodate the permanent strain observed during LVI tests. The final value of  $\beta$  was found to be 4.6 and it is worth mentioning that value remained consistent for all the conducted LVI analyses regardless the variation of the deliberate imperfection, since the material system was same and in order to limit the effect of the fitting parameters on the prediction capabilities of the model.

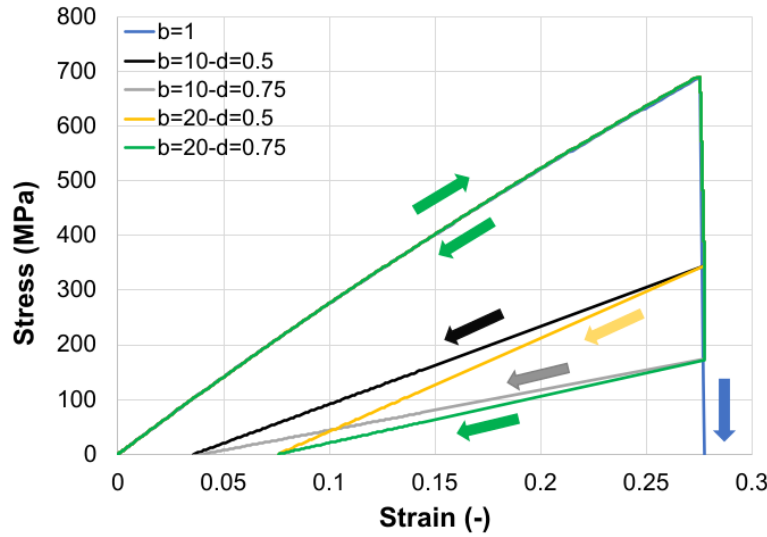


Figure 9.26: Loading and unloading of a single continuum shell element with permanent strain constants  $\beta = 1$ ,  $\beta = 10$  and  $\beta = 20$ , in-plane stress versus strain.

### 9.3.3 Sensitivity Study

In Chapter 5 a smearing crack formulation was presented to alleviate mesh dependence, however, a convergence issue in FE formulations is commonly observed because of the volume of damaged material [231]. Hence, a sensitivity analysis was employed to investigate a range of mesh densities and number of allocated CZs; identify the optimal combination of these two parameters that ensures convergence of results and then, with the optimal configuration to perform the LVI simulations. The global in-plane mesh size for the impact plate was set to 3 mm and refined around the impact region as shown in Fig. 9.27. The examined mesh densities were 3 mm, 2 mm and 1 mm, combined with configurations of 3, 7 and 15 CZs, symmetrically allocated to the midplane of the plate (Fig. 9.28). Specifically, in the FE model with 3 CZs, the zones were allocated between blankets 4 and 5, 8 and 9 and 12 and 13, in the second model with 7 CZs, they were allocated between blankets 1 and 2, 4 and 5, 5 and 6, 8 and 9, 11 and 12, 12 and 13 and 15 with 16. In the last case, CZs were assigned at all interfaces between the blankets.

Table 9.3 summarises the combinations of the sensitivity cases. The reference experimental data, against which the sensitivity cases are compared is the 30 J LVI test without deliberate imperfections, namely plate "A1" from laminate RTM\_1.



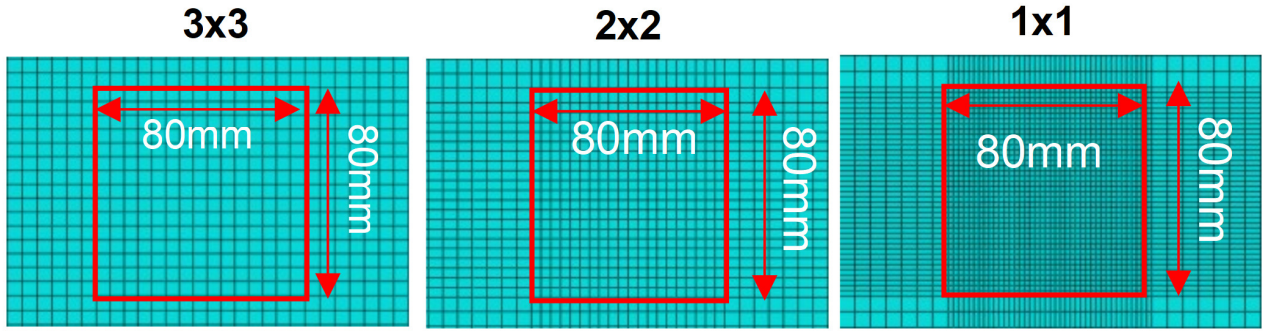


Figure 9.27: Mesh refinement and location in plate FE model.

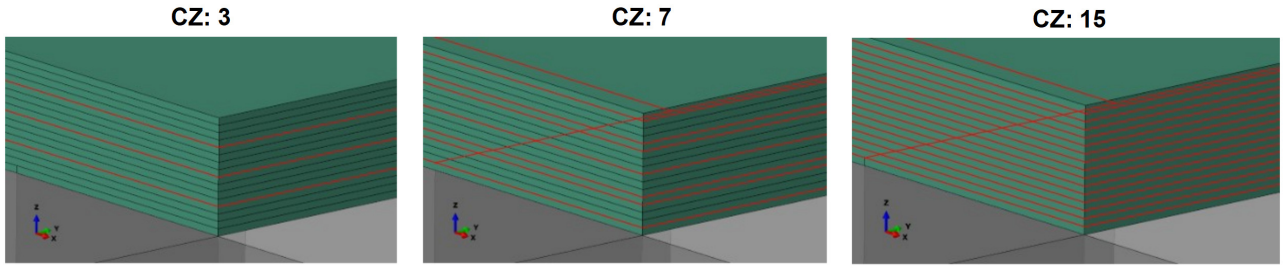


Figure 9.28: Cohesive Zone allocation in plate FE model for the sensitivity analysis.

|                |    | Element Size (mm) |     |     |
|----------------|----|-------------------|-----|-----|
|                |    | 3                 | 2   | 1   |
| Cohesive Zones | 3  | 1.1               | 2.1 | 3.1 |
|                | 7  | 1.2               | 2.2 | 3.2 |
|                | 15 | 1.3               | 2.3 | 3.3 |

Table 9.3: Summary of the sensitivity cases as a combination of mesh size and number of CZs.

In Figs. 9.29 and 9.30 the load-time and displacement-time responses are presented for 30 J LVI using three different mesh sizes for three distinct numbers of CZ. The FE results reveal convergence with the experimental response as the mesh is refined and the number of CZs is increased. Particularly, in the displacement-time response it can be observed that the permanent strain is higher (deviate from the experimental results) for the configuration with 3 CZs which implies that delamination is concentrated and extended solely to these CZs due to the absence of additional interfaces, which would allow a more even distribution of interlaminar failure.

Also, it is apparent that while the number CZs is increased, the oscillations after damage initiation are reduced, resulting in a noticeable difference in the load-time curve. This is explained by the fact that NCF composites are prone to interlaminar matrix failure (delamination) when

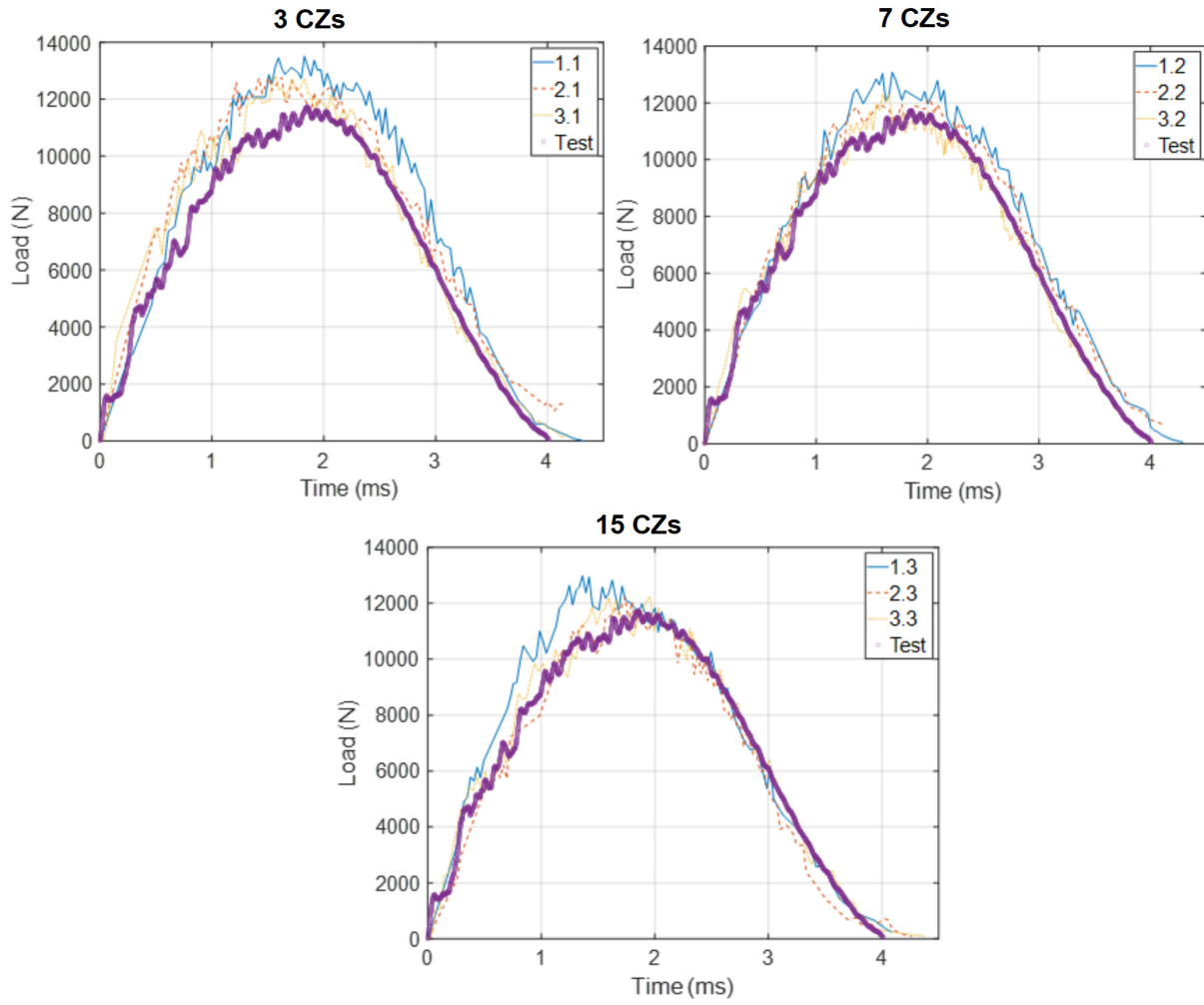


Figure 9.29: Sensitivity study, on mesh size and number of cohesive zones for 30 J impact, load versus time.

subjected to LVI as explained in Section 9.2, and this damage mode absorbs an accountable amount of energy.

As expected, the above mentioned behaviour was also expressed in the plots of kinetic energy-time (Fig. 9.31) and energy-time (Fig. 9.32), where the configurations with coarser mesh and smaller number of CZs (i.e. 1.1, 1.2 and 1.3) exhibit significantly lower amount of energy absorption and higher value of kinetic energy during the rebound of the impactor.

Accounting the findings above, considering also the research interest of the author to simulate interlaminar failure at each interface between the NCF blankets, and since the numerical simulation was not prohibitively long (app. 7 hours on HPC), the finer configuration, namely 1 mm refined mesh with 3 mm global mesh and 15 CZs (model 3.3), was selected to be utilised

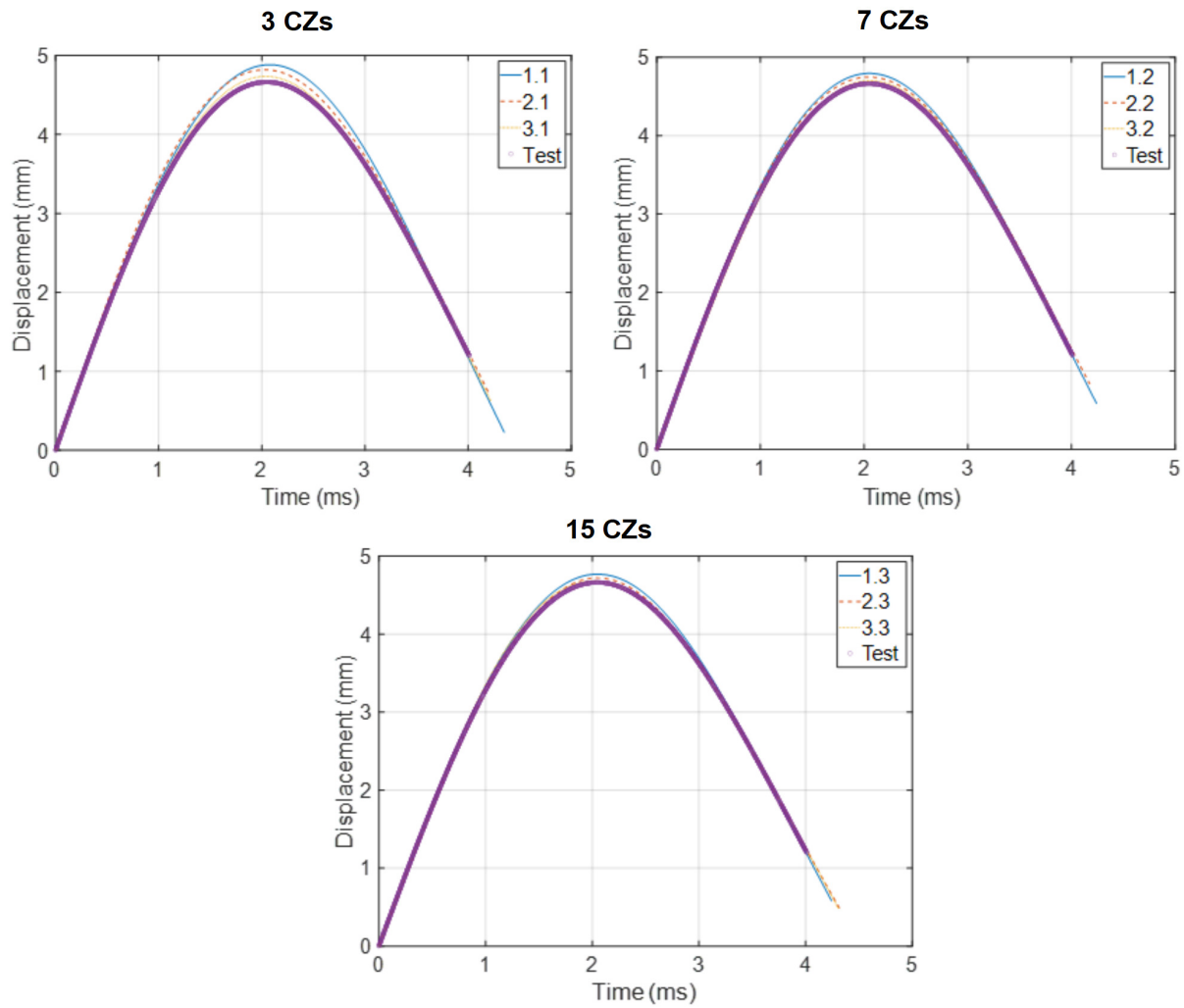


Figure 9.30: *Sensitivity study, on mesh size and number of cohesive zones for 30 J impact, displacement versus time.*

for all LVI simulations. At this stage it has to be mentioned that models 2.2, 3.2 and 2.3 correlate well with the experimental results in any expression (i.e., load-time, displacement-time, kinetic energy-time and energy-time) however the fact that the mesh is coarser limits their ability to accurately capture on the plate the developed failure patterns, therefore none of these configurations was selected to carry out the LVI analyses.

### 9.3.4 FE Results

#### 9.3.4.1 Case A - Misaligned NCF Blankets

The prediction capability and validation of the constitutive model is presented in the current subsection. Figs. 9.33 demonstrate the comparison of numerical and experimental load-

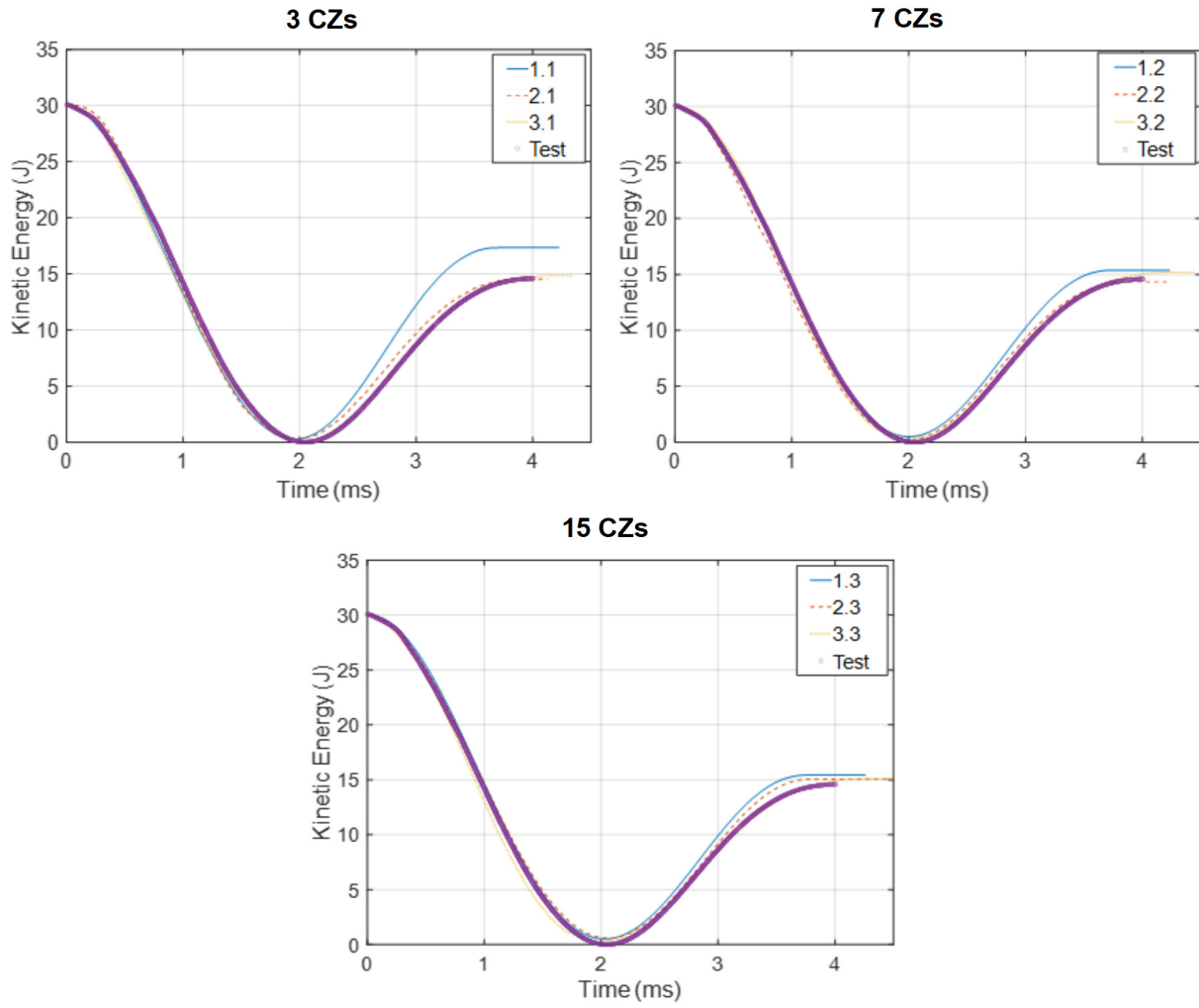


Figure 9.31: Sensitivity study, on mesh size and number of cohesive zones for 30 J impact, kinetic energy versus time.

displacement curves at four energy levels of the three misaligned configurations. In all cases the stiffness of the plates, maximum displacement and the permanent strain are in good agreement with the respective experimental data. This could be attributed to the following reasons: a) The stiffness and maximum displacement are highly dependable to the effective longitudinal stiffness,  $E_{11_{Eff}}$ , meaning that the proposed formulation of knock-down factor to calculate this magnitude performed well. And b) the permanent strain is defined by the respective parameter,  $\beta$  for which the correct value was set. However, at this stage it has to be mentioned that selecting the correct value of  $\beta$  is a tedious and sensitive procedure since a higher than the optimal value will make the plate more compliant, with reduced peak load and impact energy.

The same observations were made for the load-time curves, where particular focus was given to

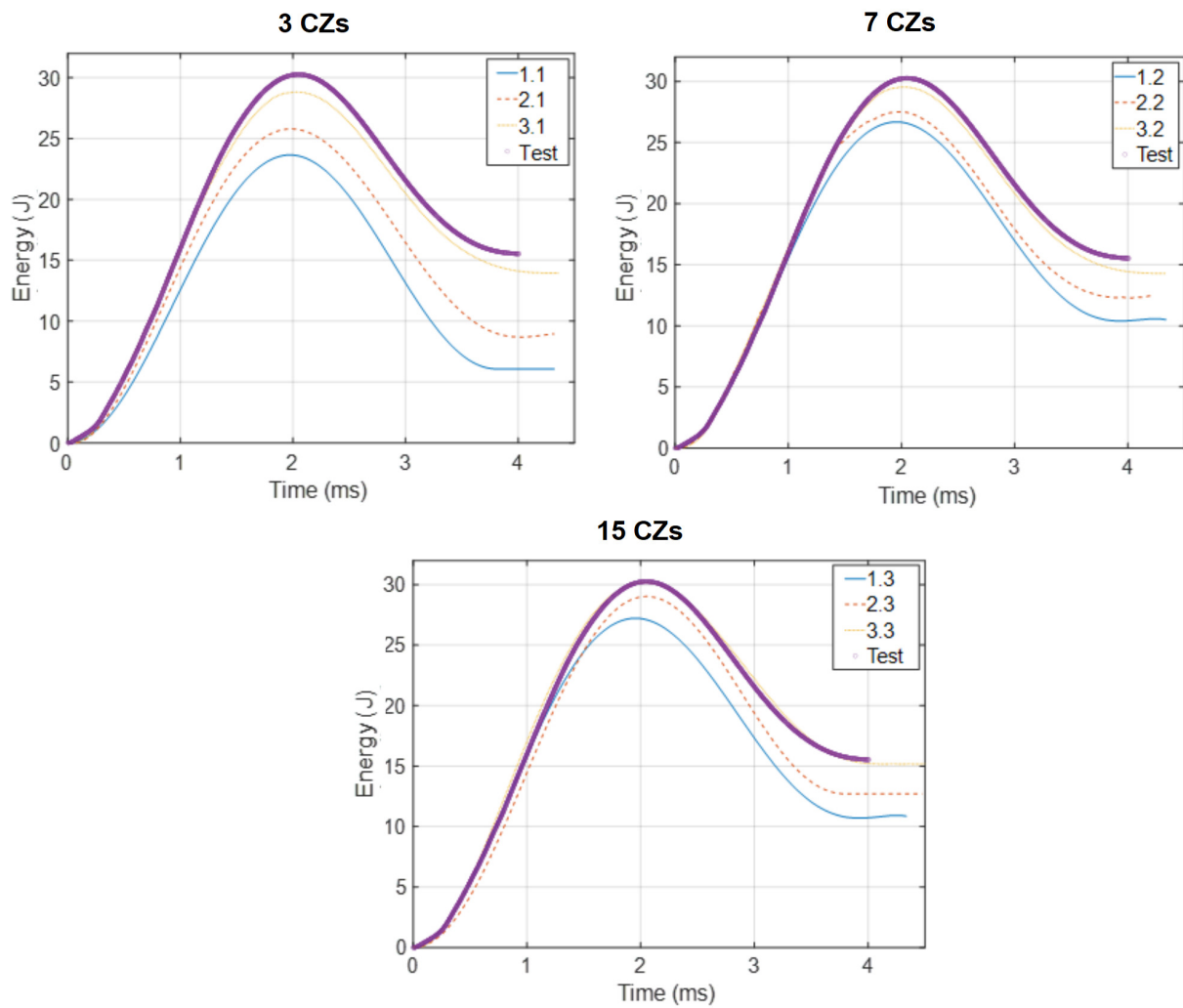


Figure 9.32: *Sensitivity study, on mesh size and number of cohesive zones for 30 J impact, energy versus time.*

the oscillations occurring at the loading path towards the maximum load. Although, they are within the acceptable limits and not excessively high, their appearance is a combined result of the vibration of the plate, damage initiation (matrix failure) and decohesion between adjacent blankets. However, this phenomenon was restrained thanks to the small amount of artificial damping that was added globally to the numerical model and the increased viscosity coefficient applied on the CZ modelling. With regards to the oscillations generated by CZ failure, it would be very difficult to eliminate them, since the decohesion of the cohesive surfaces is more rapid when compared to the cohesive element formulation.

The peak loads in the numerical simulations are slightly higher than in the experimental response. This is expected, since the numerical model does not account localised imperfections

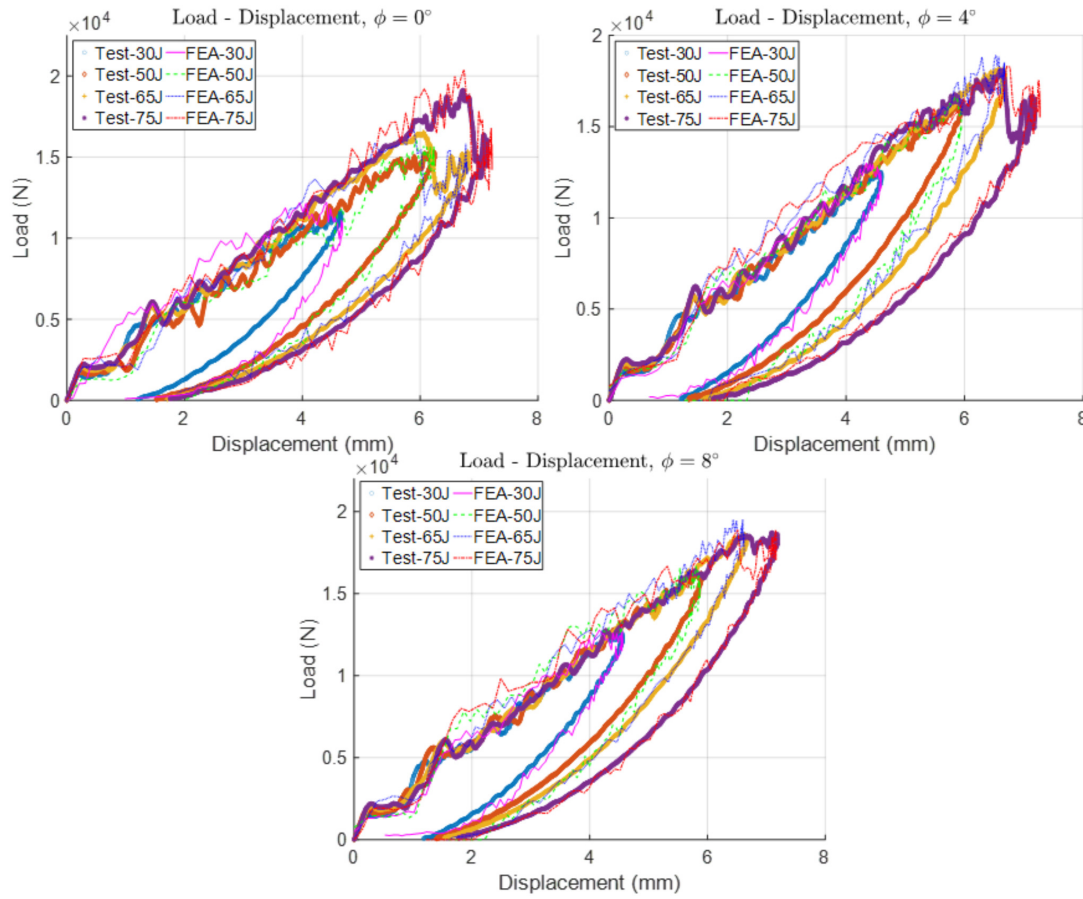


Figure 9.33: Load - Displacement curves comparison between FE models with  $\phi_I = 0^\circ$  (Top left),  $\phi_I = 4^\circ$  (Top right),  $\phi_I = 8^\circ$  (Bottom) and test results at all energy levels.

of the fibres with regards to their strength, hence as it was also seen in the tensile FEA case (Chapter 7), the tensile strength was overestimated by a margin of 4 to 5%. Still though, the fibre breakage was captured well for the models of  $\phi_I = 0^\circ$  and  $\phi_I = 4^\circ$  at 65 J and 75 J.

In Fig. 9.35 the impact energy-time curves are shown subjected to four energy levels for the three configurations. It is shown that the maximum impact energy is predicted very accurately in all cases both in terms of absolute value and correct timing. The absorbed energy though, is always smaller in the FE models in comparison to the experimental responses, which could be attributed to damage mechanisms that were not predicted by the model, such as the failure between the layers of the NCF blankets, which in this project are considered perfectly bonded. Another reason regards the compliance of test rig which also absorbs some energy to the external surroundings which again cannot be captured by the numerical model due to the rigid boundary conditions.



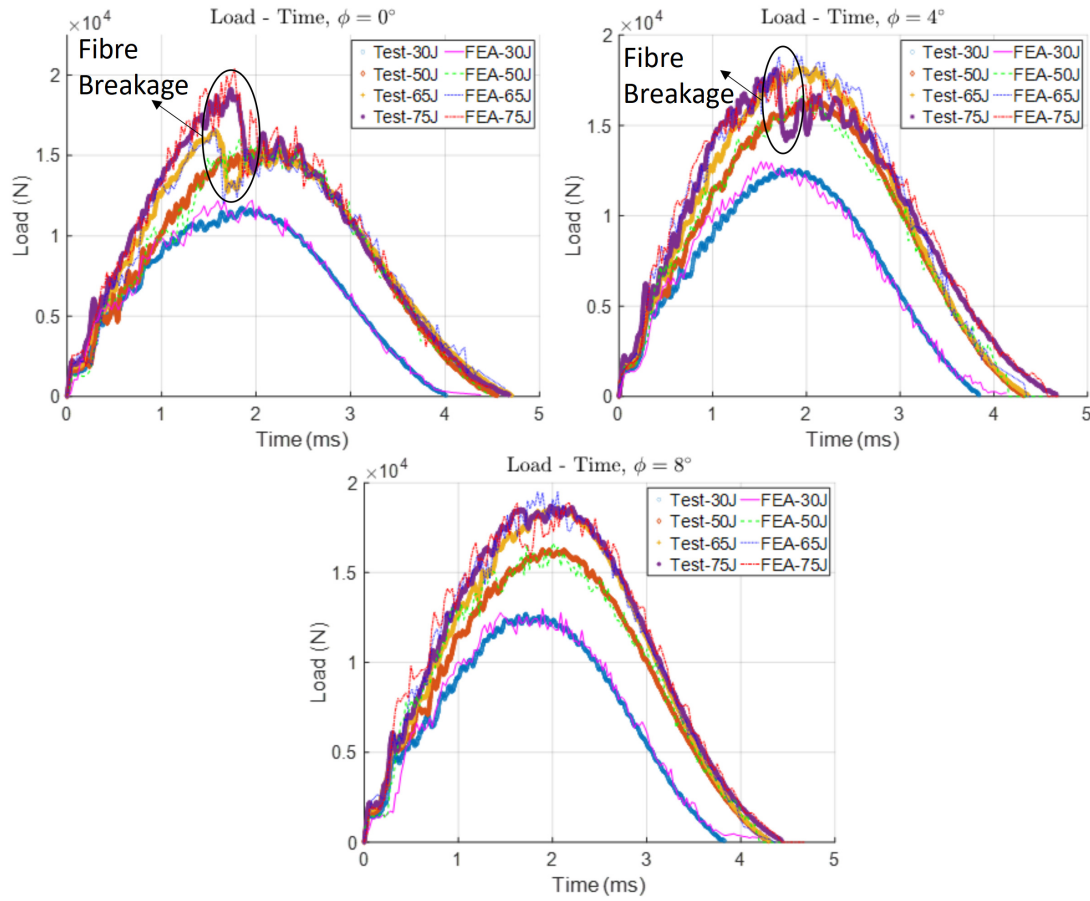


Figure 9.34: Load - Time curves comparison between FE models with  $\phi_I = 0^\circ$  (Top left),  $\phi_I = 4^\circ$  (Top right),  $\phi_I = 8^\circ$  (Bottom) and test results at all energy levels.

Fig. 9.36 demonstrates the LVI process of a numerical model for a misaligned plate of  $\phi_I = 8^\circ$  under impact energy of 75 J, in six discrete time steps. The overall duration of the event is 4.6  $\mu$ s, where a) is the initial stage of the event when no damage arises in the laminate, b) is when the impact force is as high as 13.5 kN, stress wave is generated and spreads along the in-plane directions and through-the-thickness of the specimen. Points c) and d) correspond to the stages that matrix damage has already initiated and propagates until peak load is reached, whereas at points e) and f) the impact force decreases gradually and the impactor rebounds to the initial position. The bending deformation of the plate is apparent during b) - e) whereas in f) it is partially recovered since permanent deformation is set.

In Fig. 9.37 a side by side comparison is presented between the experimentally detected via ultrasound C-Scan, delamination between blankets 12 and 13 and the respective prediction of the numerical model for all configurations of misalignment and at all impact energy levels. The

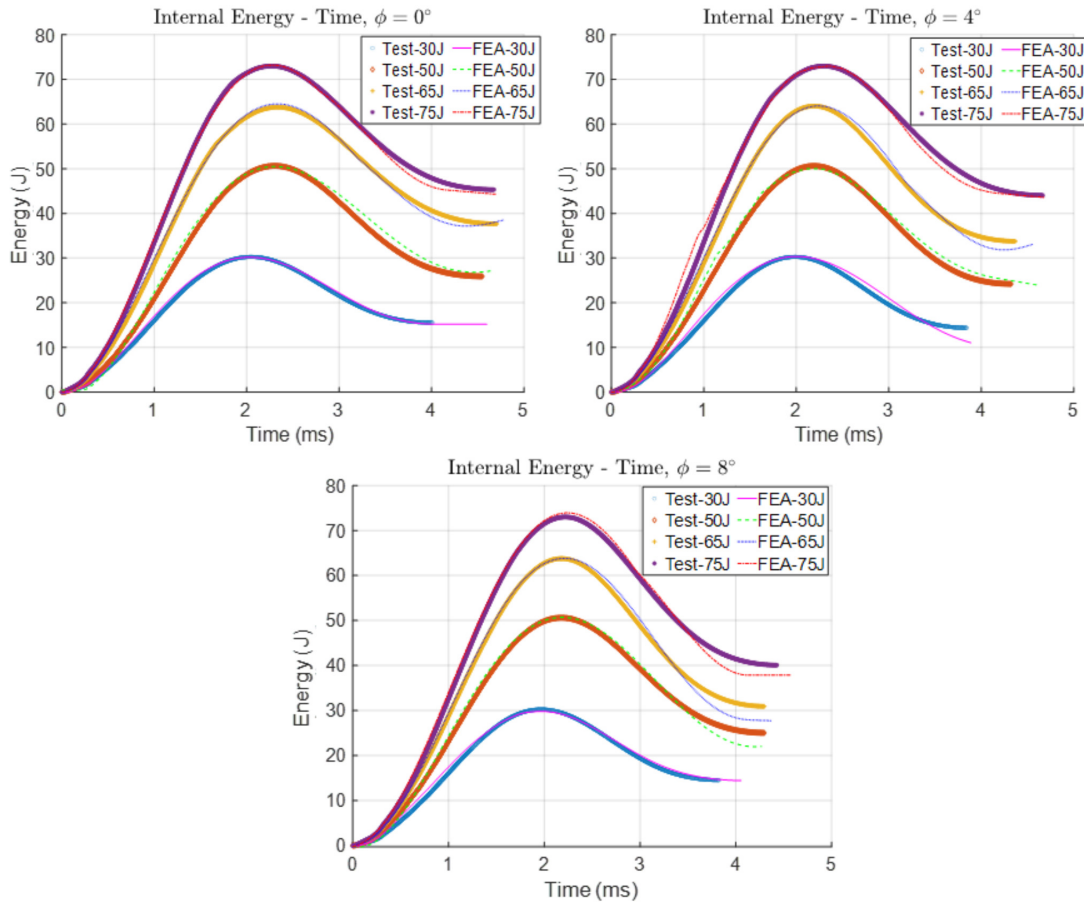


Figure 9.35: *Energy - Time curves comparison between FE models with  $\phi_I = 0^\circ$  (Top left),  $\phi_I = 4^\circ$  (Top right),  $\phi_I = 8^\circ$  (Bottom) and test results at all energy levels.*

FE model predicted well that the delamination damage occurs around the indentation region. It can also be observed that for low (30 J) and moderate (50 J) impact energy levels there is good representation of the predicted delamination for all configurations both in terms of the absolute value of the delaminated area and the pattern of decohesion. The ellipsoid shape of failure is represented satisfactorily. At higher energy levels though, namely 65 J and 75 J, the area of delamination is increased and the shape of delamination changes to irregular ellipsoid or even to rectangle. The numerical models though do not appear to capture identically the C-scan findings. There are a couple of reasons that could cause this discrepancy. Firstly, the numerical model does not incorporate and in fact homogenised all the little resin rich areas within the NCF laminate which are the root of the interlaminar failure. Secondly, the bilinear cohesive law is probably not sufficient to account the complicated damage mechanism which occur during interlaminar failure, namely stitch and fibre bridging. One last reason could regard the use of cohesive surfaces in structured meshes. Since the cohesive zone method is



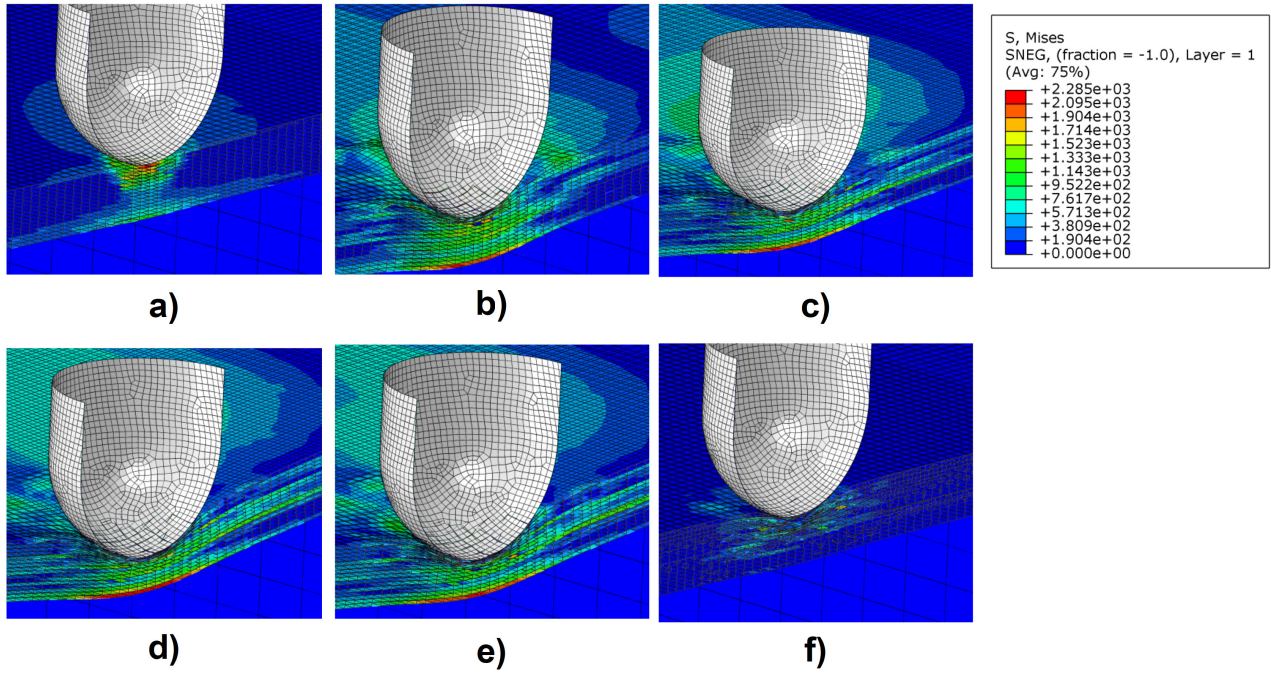


Figure 9.36: *Illustration of the numerically modelled LVI process for  $\phi_I = 8^\circ$  plate under 75J impact energy, at: a)  $t = 0.027 \mu s$ , b)  $t = 1.0 \mu s$ , c)  $t = 1.4 \mu s$ , d)  $t = 2.0 \mu s$ , e)  $t = 3.5 \mu s$  and f)  $t = 4.6 \mu s$ .*

highly mesh dependant, although an attempt was made to alleviate it, the different ratios of the in-plane dimensions of an element in certain locations of the plate can result in premature decohesion, affecting the entire failure pattern. To limit this phenomenon, specifically adjusted values of traction (interlaminar strengths) are required.

#### 9.3.4.2 Case B - Split NCF Blankets

Concerning Case B, Figs. 9.38 and 9.39 show the load-displacement, load-time and impact energy-time histories for the experiment and numerical predictions for the split blanket configuration. The numerical analysis for the uniform blanket configuration was practically presented in Case A result sections.

The numerical results in Fig. 9.38 (left and right) show very good agreement with the experimental curves of the split blankets. Similar observations as for the previous case are made, namely the slope of the loading curves does not deviate significantly from the experimental curves, as well as the peak loads in the numerical analysis are somewhat higher than in the tested plates response.

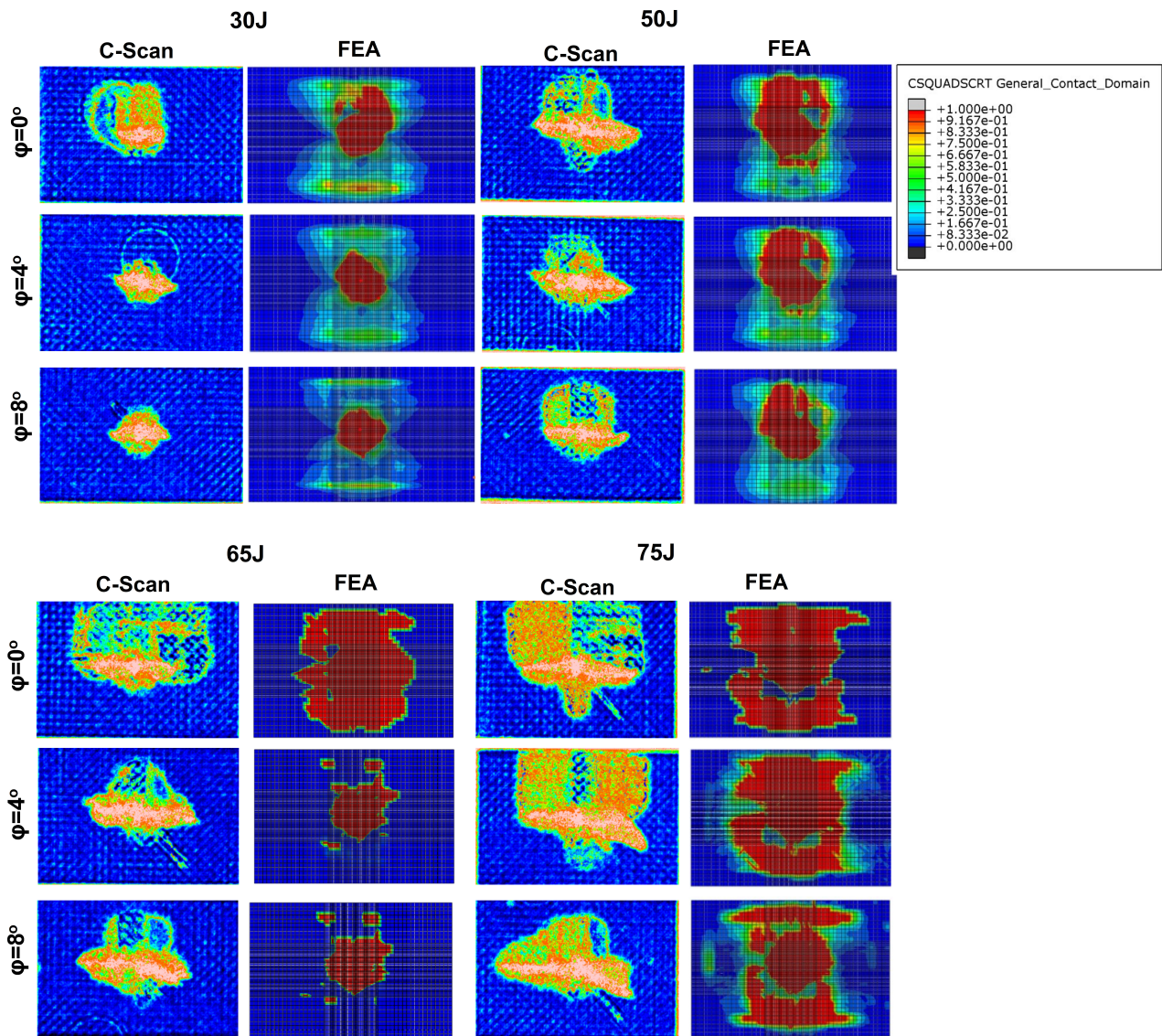


Figure 9.37: Predicted delamination between blankets 12 and 13 compared against ultrasound C-Scan observations, for all configurations and energy levels.

The impact energy-time history is presented in Fig. 9.39. In this case too, the maximum impact energy is predicted with good accuracy but with a slight, almost negligible, delay. Also, the absorbed energy in the FE models matches well with the experimental responses.

Similar observations to Case A are drawn for the prediction of delamination between blankets 12 and 13, namely the interface between a uniform (blanket 12) and a split blanket (blanket 13). The location and the shape of delamination as predicted by the numerical model agrees well with the C-Scan observations for all energy levels as illustrated in Fig. 9.40. The semi-transparent white dashed lines of the FEA contours in Fig. 9.40 indicate the interface between the two halves

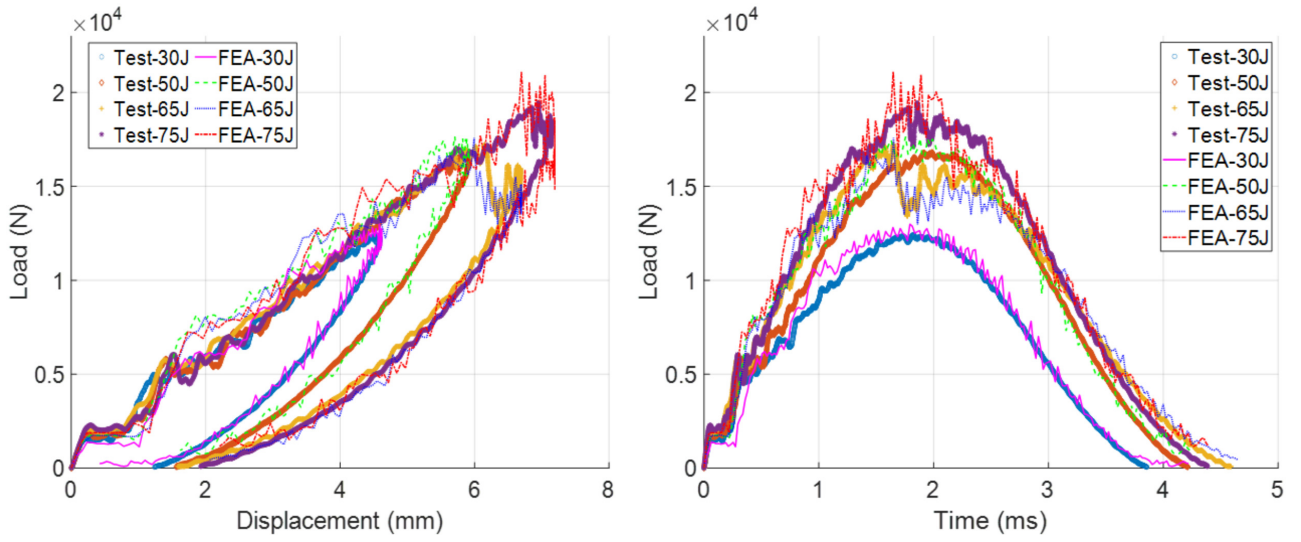


Figure 9.38: *Load - Displacement (left) and Load - Time (right) curves comparison between split blanket FE models and test results for all energy levels.*

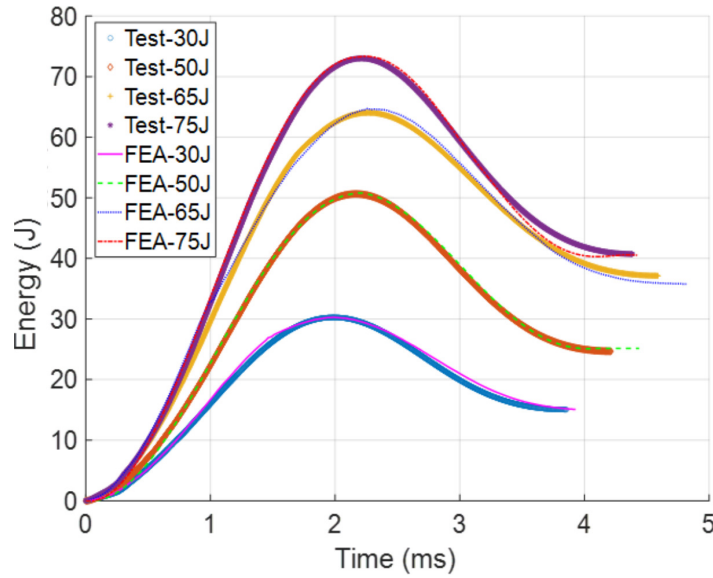


Figure 9.39: *Energy - Time curves comparison between split blanket FE models and test results for all energy levels.*

of the NCF blanket. However, when impact damage increases, undesired decohesion is observed at the regions of the plate where the element size changes. The undesired delamination does not significantly affect the mechanical response of the plate, however it absorbs an amount of internal energy which could potentially affect the prediction of other damages within the elements. Moreover, the calculated projected delaminated area of the FE models appears to be smaller than the one of the actual specimens ranging from 10% up to 15%, with a tendency to increase with the increase of the impact energy. The same reasoning regarding cohesive zone



modelling as explained in Case A is applied for this case too.

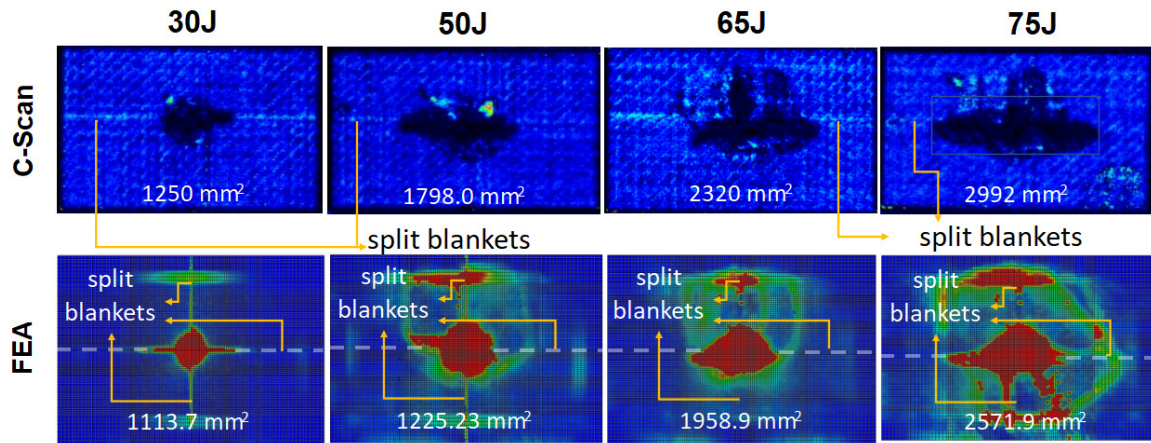


Figure 9.40: Predicted delamination between blankets 12 and 13 compared against ultrasound C-Scan observations, for split blanket at all energy levels.

The matrix damage index of the numerical model for blanket 13 (split blanket) is shown in Fig. 9.41 and compared against the C-scan findings. This comparison shows a good agreement between the numerical model predictions and the experimental observation. Matrix cracking appears to be the dominant failure mechanisms while fibre breakage is limited and only at 65 J and 75 J. The propagation of matrix damage along the interfaces between the two halves of the split blanket (white dashed line) is predicted successfully, which validates the hypothesis that it is significant source of energy absorption.

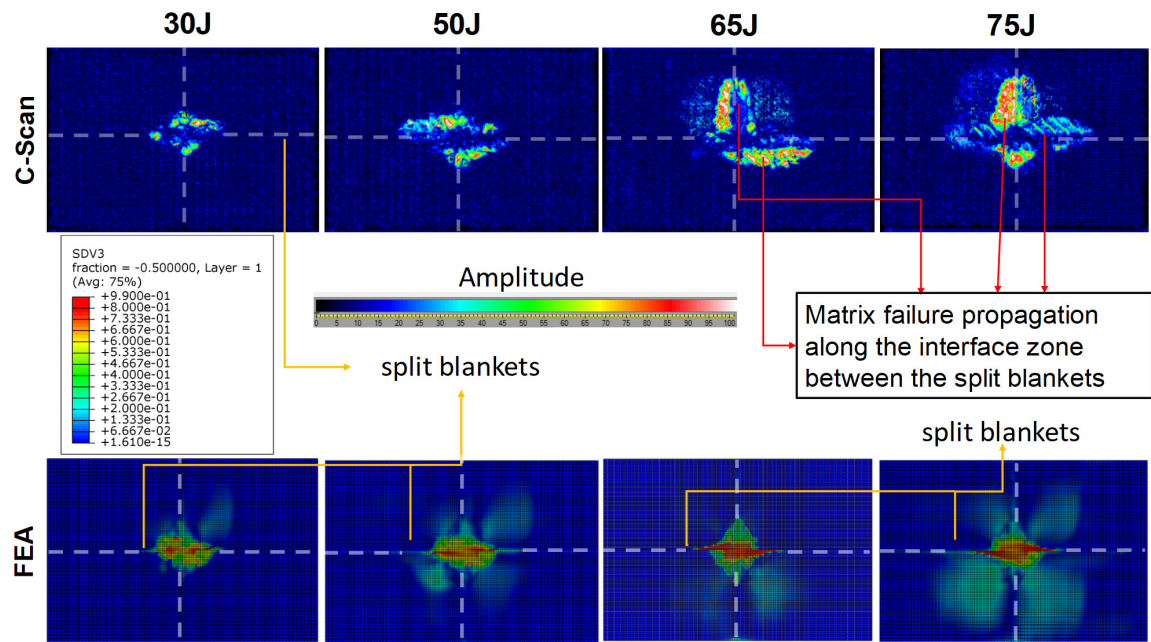


Figure 9.41: *Damage prediction of the numerical model for the split blanket configuration as compared against experimental observations from ultrasound C-scan at all energy levels.*

# Chapter 10

## Conclusion and Future Work

### 10.1 Summary

The current project aimed to present an integrated study specifically devoted to the experimental comprehension and the numerical modelling of cross-ply NCF composites. The stages of the study were divided in such way so that firstly the particular internal architecture of cross-ply NCF composites is investigated and characterised, then an extensive experimental campaign was performed, through which their mechanical properties were obtained and the corresponding failure mechanisms were examined. The experimental plan was to cover the mechanical response of NCF composites under quasi-static in-plane loadings, interlaminar and translaminar failure tests and impact tests, and correlate it with internal architecture parameters of NCF composites (e.g. stitch reinforcement) or structural imperfections (e.g. in- and out-of plane waviness). Also, a pool of experimental data was generated for the same material system and hence consistency was achieved.

Based on the experimental findings, a constitutive model was developed following the principle of predictive virtual testing, which implies the minimum use of curve fitting parameters to assist the prediction of the mechanical response of a coupon or component, and the avoidance using reverse engineering methods in order to characterise the material properties. The constitutive

model was validated against all the already performed mechanical tests and hence an holistic character of this study was achieved.

## 10.2 Conclusions

A summary of the main outcomes of the research is presented below:

### 10.2.1 Experimental Study

The internal structure of a cross-ply NCF composite is the key feature which ultimately determines its overall mechanical behaviour. The author, during the experimental study did not identify structural imperfections in terms of defects or formation of voids. However, fibre misalignment in- and out-of-plane is a characteristic feature of NCF composites. The average out-of-plane fibre misalignment was found to be 1.9 - 2.0 times higher than the average in-plane, which was explained by the fact that the latter is dominated by the stitch reinforcement pattern.

The NCF laminates were fabricated with RFI and RTM methods and it was observed that for the RTM laminates the out-of-plane misalignment was app. 17% lower than for the RFI laminates. Nevertheless, the most important conclusion according to author's observations, was the uncertainty resulted from the wide scatter of the recorded fibre angular misalignment, particularly out-of-plane. The periodicity of fibre undulation was noted to be very limited and could differ not only from specimen to specimen, obtained from the same laminate, but also from layer to layer of the same specimen. The uncertainty factor lies also on the fact that the characterisation of the fibre undulation was obtained from the cross-sections of the specimen, meaning that fibre misalignments within the specimen are completely unknown, which though could trigger failure mechanisms under certain loadings.

The stitch reinforcement as part of the internal structure of an NCF composite, enhances the interlaminar fracture toughness in peel and shear due to the formation of the stitch bridging zone supplementary to fibre bridging. This mechanisms prevents from excessive delamination

like in LVI scenarios. Also increases the shear modulus by app. 6% when the stitches are aligned to tension-shear load direction for a  $45^\circ$  layer with tricot pillar stitch pattern. However, the tension in the binding of the stitches is the reason why in-plane fibre misalignment is formed and the formation of resin pockets contribute as failure initiation sites under tensile load.

### 10.2.2 Mechanical Testing

Moreover, in terms of the mechanical performance, in- and out-of-plane fibre misalignment contribute to the exponential reduction of the longitudinal stiffness and compressive strength of the specimen, whereas in tension the in-plane fibre misalignment is responsible for the formation of intrabundle failure of  $0^\circ$  layers. Also, during LVI cases with deliberately in-plane misaligned panels it was shown that although the energy absorption is reduced, in-plane fibre undulation has a positive contribution to the structural integrity of the plate. Scissoring effect was also noted under tension-shear loads as a result of the poor matrix-fibre bond.

### 10.2.3 Constitutive Model Validation

The developed constitutive model, was validated against quasi-static in-plane loading conditions, damage propagation and dynamic LVI cases. Specifically, the newly proposed knock-down factor composed by two separate sub-terms in function of in- and out-of-plane fibre misalignment, overestimated the longitudinal stiffness in tension with a discrepancy less than 2% while in compression the range was between 2.1-3.5%. A set of physically-based failure criteria for the prediction of the failure onset were presented and formulated in an energy-based continuum damage mechanics model following a layer-wise approach. The failure criteria were implemented in Abaqus/Explicit VUMAT subroutine. Particularly for fibre compression, a new failure index was proposed which accounted both types of misalignment.

The failure criteria were validated under tension, in-plane shear and compression loading at specimen level, against various in- and out-of-plane fibre misalignment combinations implemented in detailed and simplified numerical models. Failure was predicted at layer level assuming homogenised layers, without employing geometric features of the reinforcement to the



model. The constitutive model was able to predict successfully the tensile behaviour as well as the damage band formation of the specimen. Similarly, for the in-plane shear loading case, the numerical results were in good agreement with experiments both in terms of predicting the transition of linear to non-linear response, the formation of shear bands and the ultimate shear strength. The compression numerical analyses for several combinations of in- and out-of-plane fibre misalignment overestimated the compressive strength by an average factor of 2 - 10%, captured accurately the damage sequence of the specimen and revealed the strong influence of the newly added terms of the fibre kinking criterion as well as the higher influence of the out-of-plane fibre misalignment to fibre kinking than the in-plane.

Whilst simplified numerical models with average fibre misalignment values exhibited reasonably good agreement with the test results for tension and in-plane shear cases, that was not the case for compression which is more sensitive to fibre misalignment and strong discrepancies against detailed numerical modelling and experimental results were noted. Furthermore, material properties which are determinants for the initiation and propagation of failure, such as  $E_{33}$ ,  $G_{13}$ ,  $\nu_{13}$ ,  $G_{III_C}$  and  $Z_T$  are not always available or readily measurable.

The constitutive model was also validated against a CT scenario in which a novel exponential unloading law of element failure under fibre tension and an interactive damage propagation law of matrix tension and in-plane shear are proposed. The numerical CT model correlated successfully both with the load-displacement curve and the fracture toughness evolution response of the test. Moreover, it was proved that the exponential law during element failure captured more accurately than the conventional linear unloading law the softening behaviour of the load response due to fibre bridging and fibre pull-out. A discrepancy of 1.2% and 5% between the predicted values of translaminar fracture toughness of initiation and propagation against the respective experimental values was achieved.

Concurrently, the proposed interactive damage propagation law of matrix tension and in-plane shear simulated well the non-linear response of the loading curve prior to failure onset, which is when the 90° layers gradually fail. The damage pattern of the crack path is predicted accurately as fibre tension failure, while the examination of undesired failures in crucial areas

of the specimen (i.e., fibre compression, in-plane shear) confirms the validity of the CT test. A side by side comparison of the  $\epsilon_{yy}$  strain map between the DIC results and the FEA output, revealed a very good agreement.

#### 10.2.4 Impact

The experimental investigation on the low velocity impact behaviour of NCF composites with deliberate imperfections, showed that plates with in-plane misalignment exhibit superior ability to maintain the structural integrity of the plate, whilst plates without deliberate imperfections dissipate higher amount of energy. It was also shown that the projected delaminated area between adjacent blankets with fibre misalignment, decreases as the angle of misaligned blanket is increased. The comparative LVI case study between uniform and split blankets NCF laminates, revealed that uniform blanket plates have superior ability to maintain their integrity of the plate, while split blanket laminates dissipate higher amount of energy at all energy levels. The bending stiffness was not much affected by the split blankets. The projected delaminated area was higher for the uniform blanket plates, however excessive matrix failure was observed for the split blanket plates and particularly in the interface zone of the blanket halves.

The constitutive model predicted accurately the load response as well as the displacement for the different energy levels and both configurations. In particular, the permanent strain parameter,  $\beta$  plays a significant role to define accurately the compliance of the laminates under LVI. Also, the absorbed energy was in good correlation with the test data for all energy levels although somewhat lower by app. 2%. The delamination shapes were not capture identically to C-scan findings, particularly for impact energy levels 65 J and 75 J, whereas the projected damaged area was predicted to be smaller by app. 5 - 8% in numerical simulations when compared to C-scans.

## 10.3 Future Work

Several parameters of the current work could be improved and other directions are indicated with regards to the development of the constitutive model.

An area that requires further study is the interlaminar behaviour between the layers of the same NCF blanket. In this study, a fair assumption was made that the layers within the same blanket are perfectly bonded and they are not susceptible to interlaminar failure. Such an assumption may not affect significantly the results at coupon level numerical simulations but for more complicated structures with curved regions, this assumption is possibly not valid. Since the parameter of fabric's drapeability plays a more significant role every interface between adjacent layers need to be precisely modelled. Furthermore, the bilinear cohesive law, appears to be insufficient to model the stitch bridging effect, which is characteristic of NCF composites and crucial for dynamic or transient conditions such as the LVI, which are prone to interlaminar failure.

In this research the developed constitutive model was implemented at coupon level, however, it is worth to be applied in more complicated geometries, other than purely rectangular, namely a skin-stiffener configuration with blanket drops, and identify gaps and areas of further improvements. One improvement that the author considers necessary would be to introduce another method for mesh sensitivity alleviation instead of the smearing methodology. The reason lies on the fact that more demanding geometries require the use of additional types of finite elements, such as 6-node wedge elements, whose characteristic length cannot be calculated with a smearing method. If so, the generated failure patterns of combined loading conditions could be predicted more representatively.

Another challenge for the current constitutive model would be to examine its prediction capabilities to other types of NCF preforms such as triaxial or quadaxial NCF blankets, in which the interaction within the adjacent layers is different, the stitch reinforcement results in localised wrinkling and is root of additional damage mechanisms.

With regards to the impact tests with the deliberate imperfections, it would probably be an interesting idea to proceed with a damage tolerance assessment (Compression After Impact) or fatigue tests for complete characterisation of both configurations with imperfections.

Finally, as mentioned previously, the mechanical behaviour of NCF composites is characterised by a lot of uncertainties mainly due to their internal structure. The author believes that the deterministic approach that has been followed in this project is saturated and not many essential steps for further improvements can be made. As a result of this, and given a wealth pool of experimental data, a probabilistic approach adjoint with a supervised learning method could contribute more to the development of a constitutive model which could better predict not only the mechanical behaviour and the corresponding damage mechanisms of NCF composites, but also more realistic failure envelopes could be generated for combined loading conditions in complicated NCF structures.

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# Appendix A

## Experimental Data

### A.1 Manufacturing Equipment

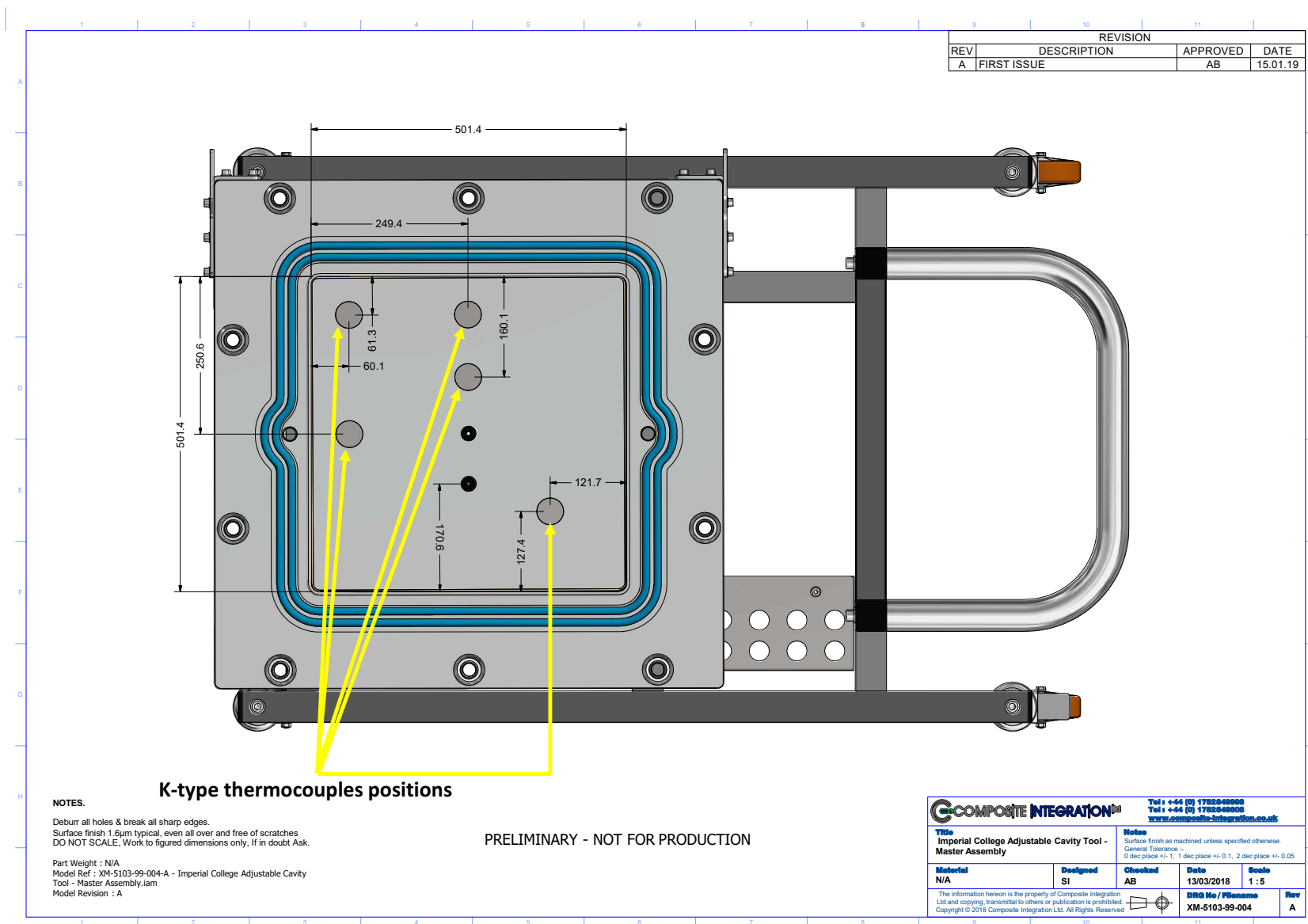


Figure A.1: Top view of the lower mould half illustrating the positioning of the K-type thermocouples, courtesy of Composite Integration Ltd.

## A.2 Specimens Dimensions

| Specimen | Thickness<br>(mm) | Gauge Length<br>(mm) | Width (mm) | Length (mm) |
|----------|-------------------|----------------------|------------|-------------|
| RFL2-T_1 | 2.26              | 146.66               | 24.67      | 249.00      |
| RFL2-T_2 | 2.17              | 146.80               | 24.46      | 249.00      |
| RFL2-T_3 | 2.16              | 147.10               | 24.50      | 249.00      |
| RFL2-T_4 | 2.19              | 146.93               | 24.62      | 249.50      |
| RFL2-T_5 | 2.19              | 145.44               | 24.63      | 248.50      |

Table A.1: *Measured dimensions of tension specimens.*

| Specimen   | Thickness<br>(mm) | Gauge Length<br>(mm) | Width (mm) | Length (mm) |
|------------|-------------------|----------------------|------------|-------------|
| RFL3-IPS_1 | 2.20              | 146.70               | 24.74      | 249.00      |
| RFL3-IPS_2 | 2.21              | 146.58               | 24.67      | 249.00      |
| RFL3-IPS_3 | 2.17              | 144.93               | 24.68      | 249.00      |
| RFL3-IPS_4 | 2.17              | 144.95               | 24.67      | 249.30      |
| RFL3-IPS_5 | 2.20              | 144.31               | 24.69      | 249.20      |

Table A.2: *Measured dimensions of in-plane shear specimens.*

| Specimen   | Thickness<br>(mm) | Gauge Length<br>(mm) | Width (mm) | Length (mm) |
|------------|-------------------|----------------------|------------|-------------|
| RFL1-C-0-1 | 2.16              | 8.00                 | 9.89       | 89.77       |
| RFL1-C-0-2 | 2.13              | 8.00                 | 9.86       | 89.74       |
| RFL1-C-0-3 | 2.18              | 8.43                 | 9.85       | 89.73       |
| RFL1-C-0-4 | 2.12              | 8.23                 | 9.86       | 89.72       |
| RFL1-C-0-5 | 2.17              | 8.07                 | 9.88       | 89.74       |

Table A.3: *Measured dimensions of compression specimens, with  $\phi_I = 0^\circ$ .*

| Specimen   | Thickness<br>(mm) | Gauge Length<br>(mm) | Width (mm) | Length (mm) |
|------------|-------------------|----------------------|------------|-------------|
| RFL1-C-2-1 | 2.31              | 8.50                 | 9.85       | 89.83       |
| RFL1-C-2-2 | 2.21              | 8.20                 | 9.86       | 89.78       |
| RFL1-C-2-3 | 2.25              | 8.25                 | 9.85       | 89.83       |
| RFL1-C-2-4 | 2.18              | 8.25                 | 9.84       | 89.76       |
| RFL1-C-2-5 | 2.19              | 8.20                 | 9.84       | 89.76       |

Table A.4: *Measured dimensions of compression specimens, with  $\phi_I = 2^\circ$ .*



| Specimen   | Thickness (mm) | Gauge Length (mm) | Width (mm) | Length (mm) |
|------------|----------------|-------------------|------------|-------------|
| RFL1-C-4-1 | 2.14           | 8.44              | 9.87       | 89.77       |
| RFL1-C-4-2 | 2.17           | 8.6               | 9.88       | 89.70       |
| RFL1-C-4-3 | 2.26           | 8.45              | 9.88       | 89.93       |
| RFL1-C-4-4 | 2.2            | 8.5               | 9.8        | 89.7        |
| RFL1-C-4-5 | 2.27           | 8.4               | 9.85       | 89.81       |

Table A.5: Measured dimensions of compression specimens, with  $\phi_I = 4^\circ$ .

| Specimen   | Thickness (mm) | Gauge Length (mm) | Width (mm) | Length (mm) |
|------------|----------------|-------------------|------------|-------------|
| RFL1-C-6-1 | 2.1            | 8.5               | 9.85       | 89.9        |
| RFL1-C-6-2 | 2.12           | 8.3               | 9.8        | 89.8        |
| RFL1-C-6-3 | 2.15           | 8.4               | 9.82       | 89.9        |
| RFL1-C-6-4 | 2.12           | 8.5               | 9.75       | 89.9        |
| RFL1-C-6-5 | 2.22           | 8.4               | 9.82       | 89.8        |

Table A.6: Measured dimensions of compression specimens, with  $\phi_I = 6^\circ$ .

| Specimen   | Thickness (mm) | Gauge Length (mm) | Width (mm) | Length (mm) |
|------------|----------------|-------------------|------------|-------------|
| RFL1-C-8-1 | 2.15           | 8.3               | 9.82       | 89.5        |
| RFL1-C-8-2 | 2.15           | 8.5               | 9.8        | 89.5        |
| RFL1-C-8-3 | 2.2            | 8.4               | 9.75       | 89.9        |
| RFL1-C-8-4 | 2.3            | 8.4               | 9.85       | 89.6        |
| RFL1-C-8-5 | 2.25           | 8.5               | 9.8        | 89.8        |

Table A.7: Measured dimensions of compression specimens, with  $\phi_I = 8^\circ$ .

| Specimen    | Thickness (mm) | Gauge Length (mm) | Width (mm) | Length (mm) |
|-------------|----------------|-------------------|------------|-------------|
| RFL1-C-10-1 | 2.1            | 8.6               | 9.8        | 89.7        |
| RFL1-C-10-2 | 2.13           | 8.6               | 9.85       | 89.75       |
| RFL1-C-10-3 | 2.2            | 8.5               | 9.86       | 89.9        |
| RFL1-C-10-4 | 2.18           | 8.55              | 9.85       | 89.85       |
| RFL1-C-10-5 | 2.17           | 8.5               | 9.82       | 89.88       |

Table A.8: Measured dimensions of compression specimens, with  $\phi_I = 10^\circ$ .

| Specimen    | Thickness (mm) | Length (mm) | Width (mm) | Pre-crack (mm) |
|-------------|----------------|-------------|------------|----------------|
| RFL4-MD I.1 | 3.45           | 149.00      | 20.43      | 58.92          |
| RFL4-MD I.2 | 3.33           | 149.00      | 20.58      | 59.29          |
| RFL4-MD I.3 | 3.31           | 149.00      | 20.64      | 59.78          |
| RFL4-MD I.4 | 3.36           | 149.00      | 20.54      | 59.38          |
| RFL4-MD I.5 | 3.37           | 149.00      | 20.42      | 59.03          |
| RFL4-MD I.6 | 3.33           | 149.00      | 20.55      | 58.49          |

Table A.9: Measured dimensions of Mode I DCB specimens.

| Specimen     | Thickness (mm) | Length (mm) | Width (mm) | Pre-crack (mm) |
|--------------|----------------|-------------|------------|----------------|
| RFL4-MD II_1 | 3.42           | 144.75      | 22.73      | 47.50          |
| RFL4-MD II_2 | 3.25           | 144.57      | 22.78      | 47.78          |
| RFL4-MD II_3 | 3.21           | 144.35      | 22.79      | 47.45          |
| RFL4-MD II_4 | 3.22           | 144.30      | 22.83      | 47.82          |
| RFL4-MD II_5 | 3.27           | 144.36      | 22.78      | 47.63          |
| RFL4-MD II_6 | 3.26           | 144.40      | 22.84      | 47.68          |

Table A.10: Measured dimensions of Mode II 4pt-ENF specimens.

| Specimen  | Thickness (mm) | Length (mm) | Height (mm) | Pre-crack (mm) |
|-----------|----------------|-------------|-------------|----------------|
| RFL5-CT_1 | 8.18           | 64.47       | 59.59       | 40.00          |
| RFL5-CT_2 | 7.69           | 64.37       | 59.38       | 40.08          |
| RFL5-CT_3 | 7.90           | 64.41       | 59.47       | 40.41          |
| RFL5-CT_4 | 7.54           | 64.45       | 59.42       | 40.50          |
| RFL5-CT_5 | 7.20           | 64.36       | 59.35       | 40.38          |
| RFL5-CT_6 | 7.81           | 64.39       | 59.45       | 40.24          |

Table A.11: Measured dimensions of CT specimens.

| Specimen     | Thickness (mm) | Length (mm) | Width (mm) |
|--------------|----------------|-------------|------------|
| RTM_1-A1-30J | 4.93           | 149.60      | 99.91      |
| RTM_1-A2-50J | 4.94           | 149.53      | 99.88      |
| RTM_1-A3-65J | 4.93           | 149.70      | 99.87      |
| RTM_1-A4-75J | 4.94           | 149.71      | 99.83      |
| RTM_1-B1-30J | 4.94           | 149.76      | 99.83      |
| RTM_1-B2-50J | 4.92           | 149.70      | 99.93      |
| RTM_1-B3-65J | 4.90           | 149.81      | 99.92      |
| RTM_1-B4-75J | 4.90           | 149.73      | 99.91      |
| RTM_1-C1-30J | 4.92           | 149.85      | 99.84      |
| RTM_1-C2-50J | 4.90           | 149.88      | 99.90      |
| RTM_1-C3-65J | 4.91           | 149.78      | 99.88      |
| RTM_1-C4-75J | 4.92           | 149.74      | 99.87      |

Table A.12: Measured dimensions of LVI specimens of RTM\_1 laminate.

| Specimen     | Thickness (mm) | Length (mm) | Width (mm) |
|--------------|----------------|-------------|------------|
| RTM_2-A2-30J | 5.05           | 149.79      | 99.85      |
| RTM_2-A4-50J | 5.02           | 149.76      | 99.86      |
| RTM_2-A5-65J | 5.00           | 149.80      | 99.85      |
| RTM_2-A3-75J | 5.01           | 149.83      | 99.90      |
| RTM_2-B1-30J | 5.04           | 149.89      | 99.85      |
| RTM_2-B2-50J | 5.03           | 149.77      | 99.86      |
| RTM_2-B3-65J | 5.01           | 149.91      | 99.89      |
| RTM_2-B4-75J | 5.00           | 149.69      | 99.89      |

Table A.13: Measured dimensions of LVI specimens of RTM\_2 laminate.

## A.3 Compliance Calibration Methods of DCB tests

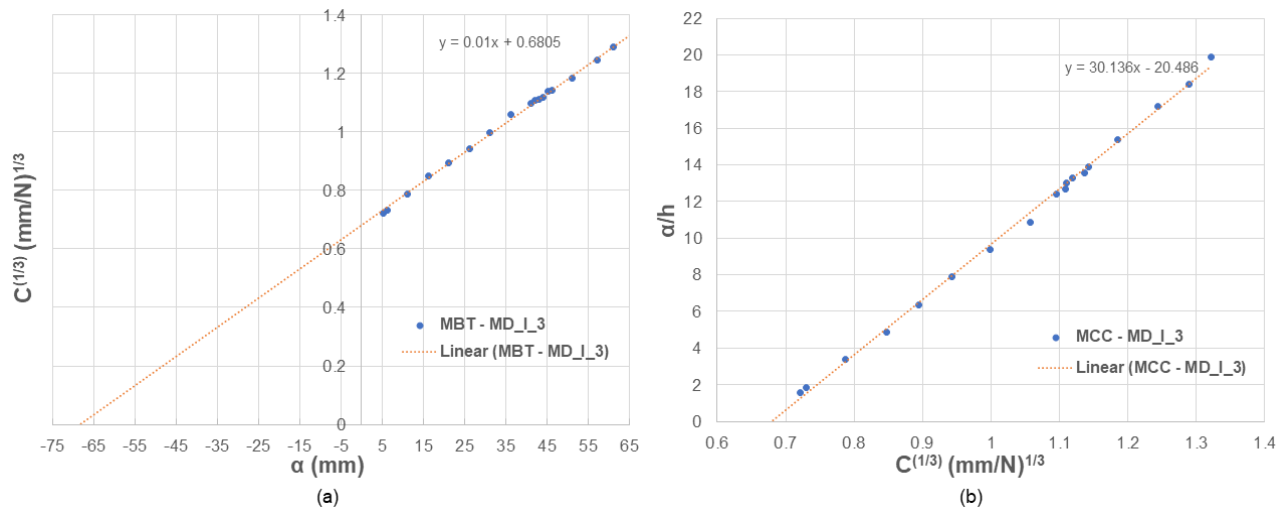


Figure A.2: Specimen MD I.2 graph of: a) Modified Beam Theory Method, b) Modified Compliance Calibration Method.

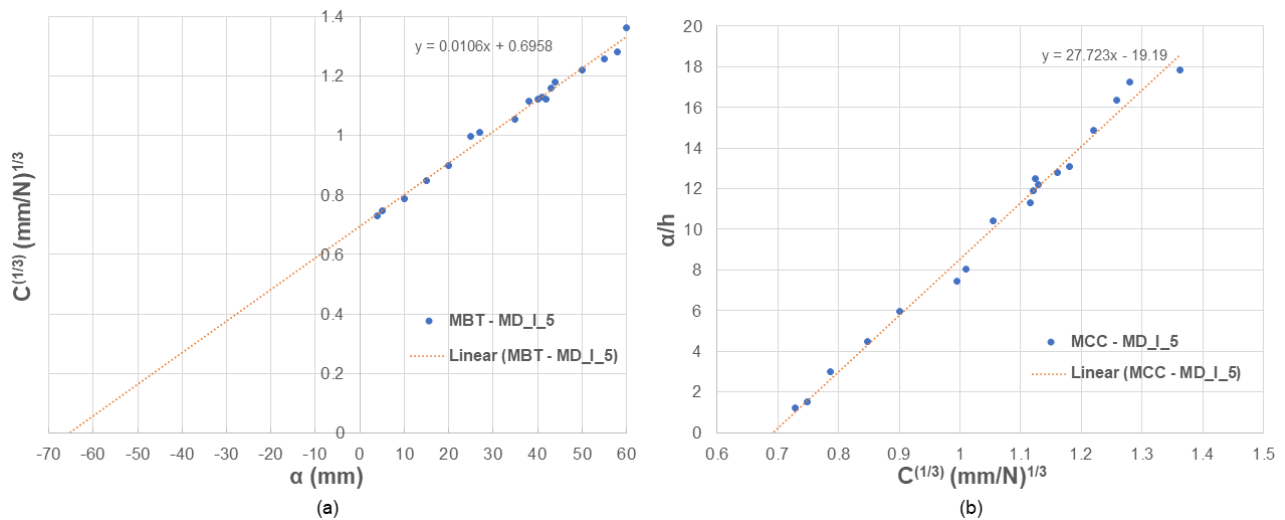


Figure A.3: Specimen MD I.3 graph of: a) Modified Beam Theory Method, b) Modified Compliance Calibration Method.

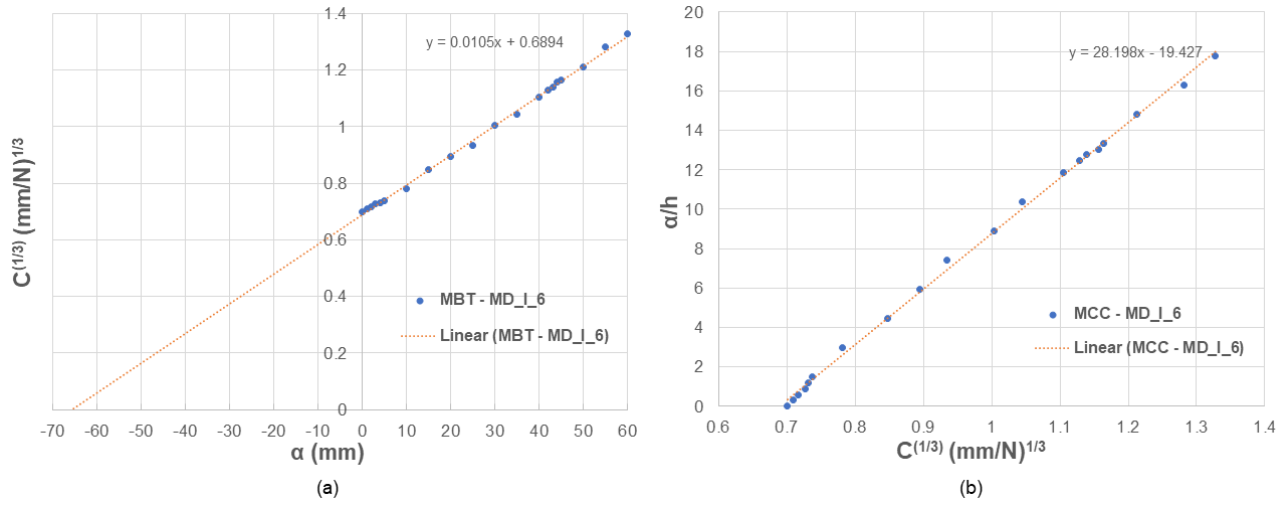


Figure A.4: Specimen MD I<sub>4</sub> graph of: a) Modified Beam Theory Method, b) Modified Compliance Calibration Method.

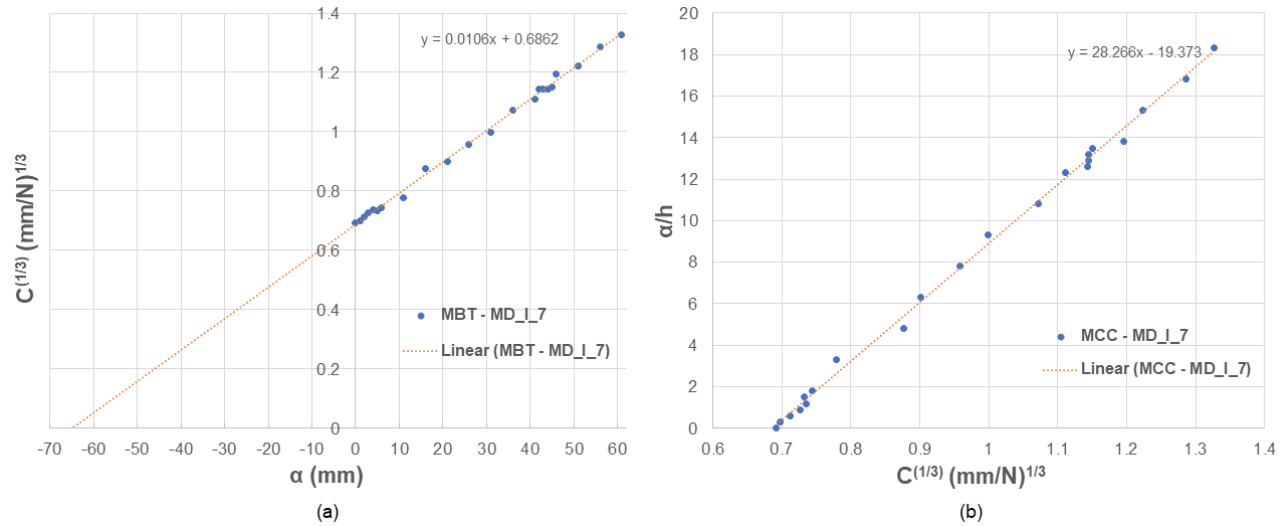


Figure A.5: Specimen MD I<sub>5</sub> graph of: a) Modified Beam Theory Method, b) Modified Compliance Calibration Method.

## A.4 Interlaminar fracture toughness, $G_{IC}$ of DCB tests

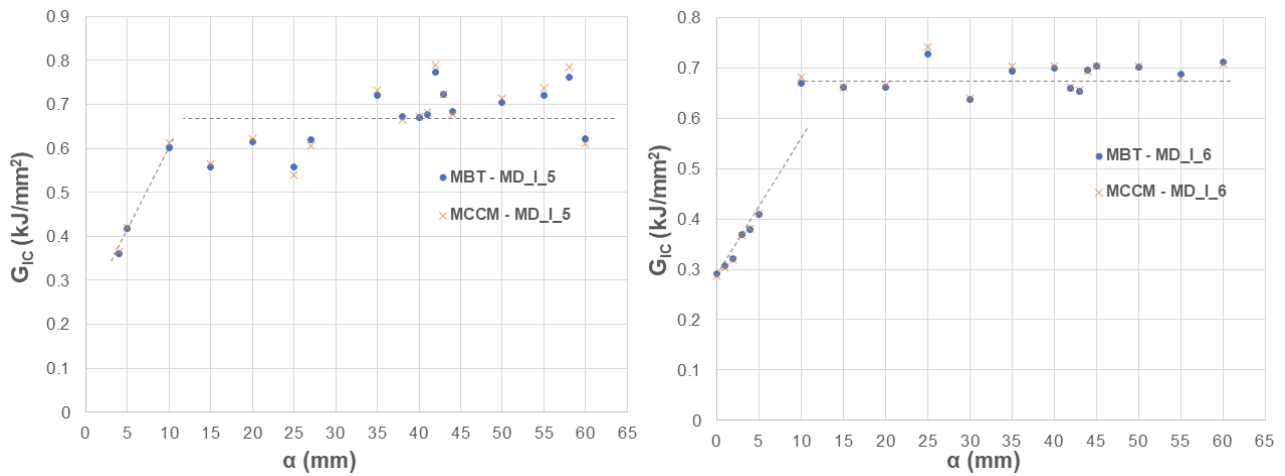


Figure A.6: Resistance ( $R$ ) curves of specimens MD I\_2 and 3, expressing the interlaminar fracture toughness versus crack length, calculated both with MBTM and MCCM.

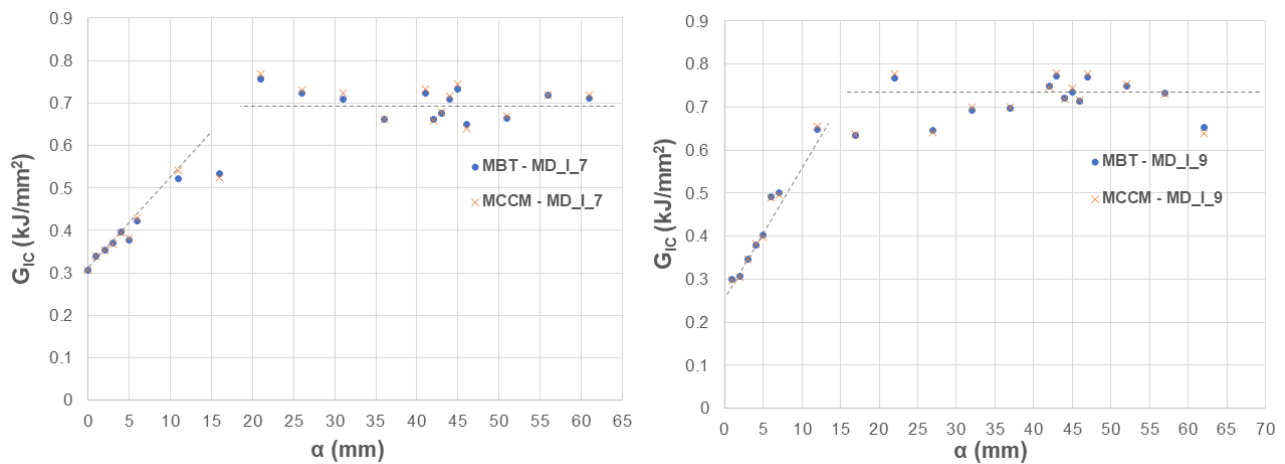


Figure A.7: Resistance ( $R$ ) curves of specimens MD I\_4 and 5, expressing the interlaminar fracture toughness versus crack length, calculated both with MBTM and MCCM.

## A.5 Compliance Calibration Method of 4pt-ENF tests

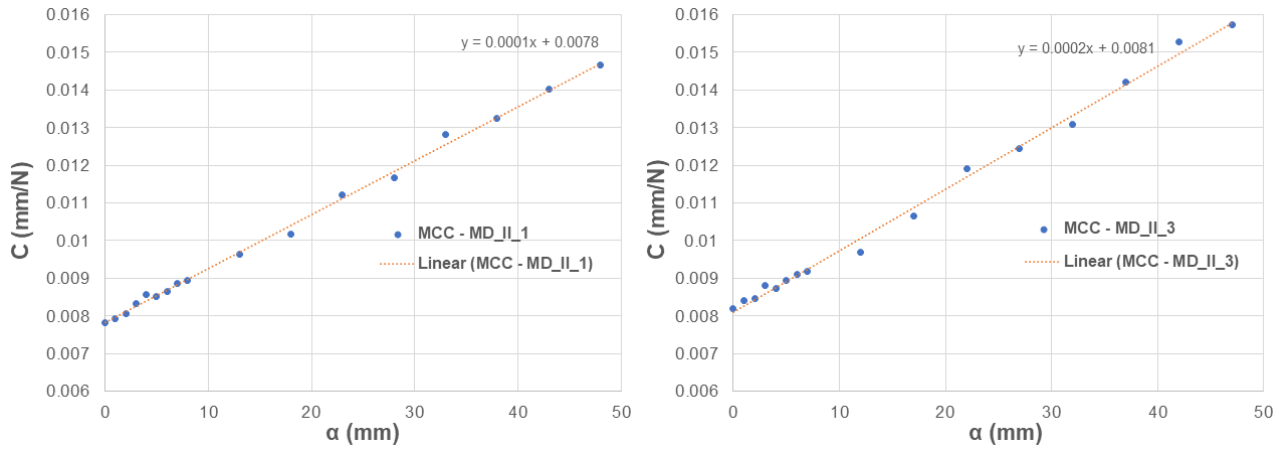


Figure A.8: *Martin and Davidson Compliance Calibration Method graph of specimens MD II.2 and 3 for the 4pt-ENF test.*

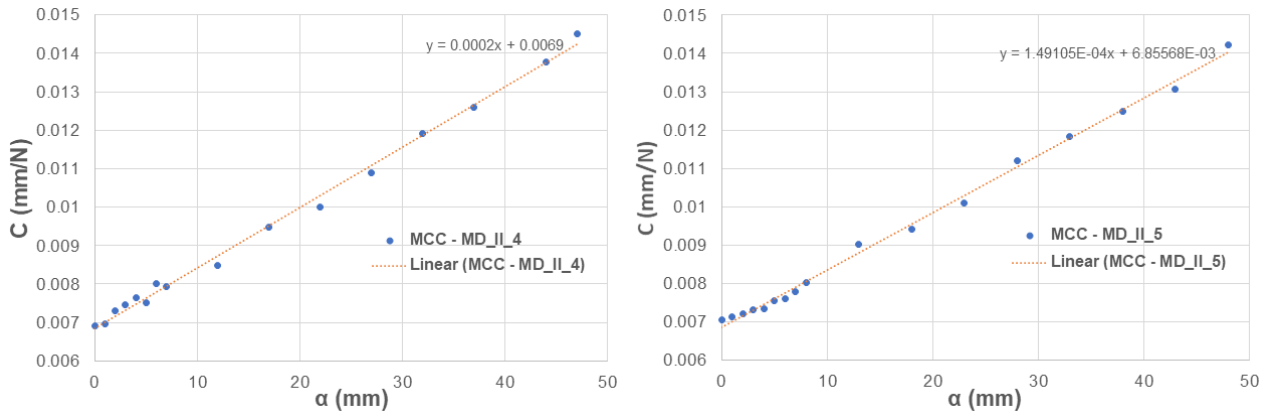


Figure A.9: *Martin and Davidson Compliance Calibration Method graph of specimens MD II.4 and 5 for the 4pt-ENF test.*

## A.6 Interlaminar fracture toughness, $G_{IIC}$ of 4pt-ENF tests

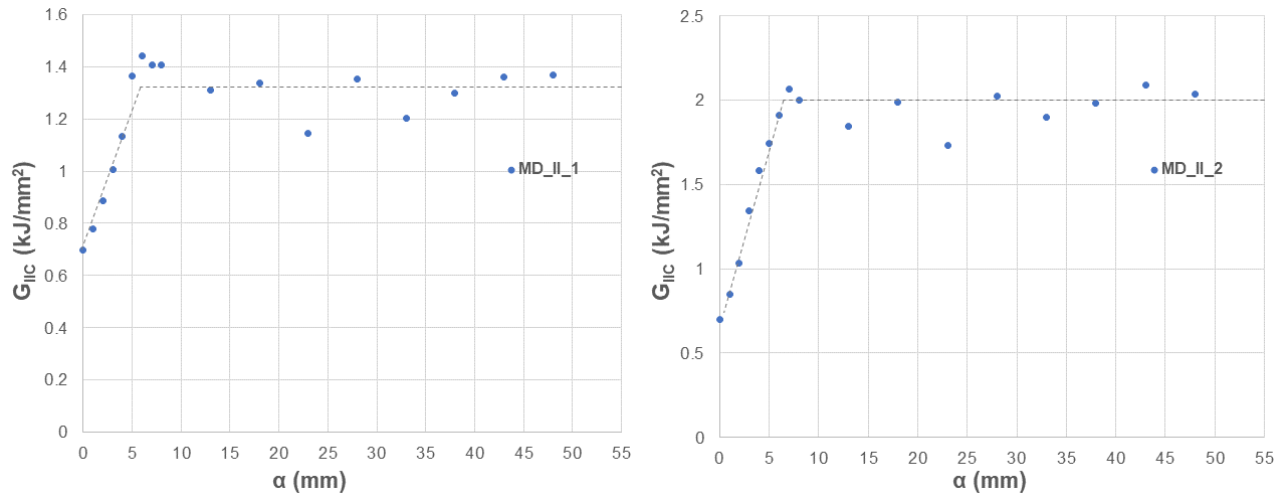


Figure A.10: Resistance ( $R$ ) curves of specimens MD II\_2 and 3, expressing the interlaminar fracture toughness versus crack length, calculated with the MCCM.

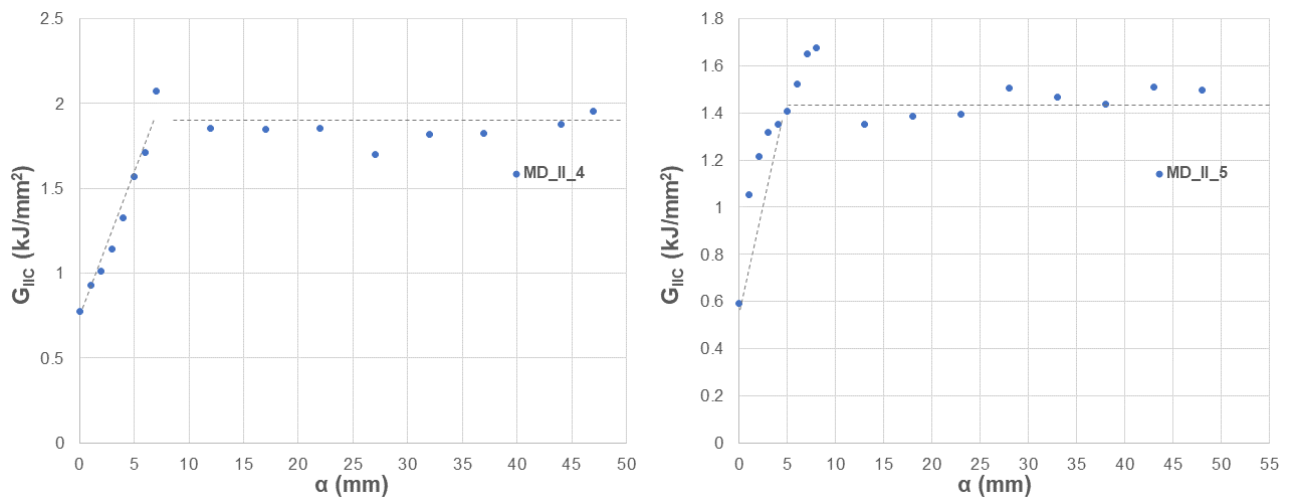


Figure A.11: Resistance ( $R$ ) curves of specimens MD II\_4 and 5, expressing the interlaminar fracture toughness versus crack length, calculated with the MCCM.

## A.7 Compliance Calibration Method of CT specimens

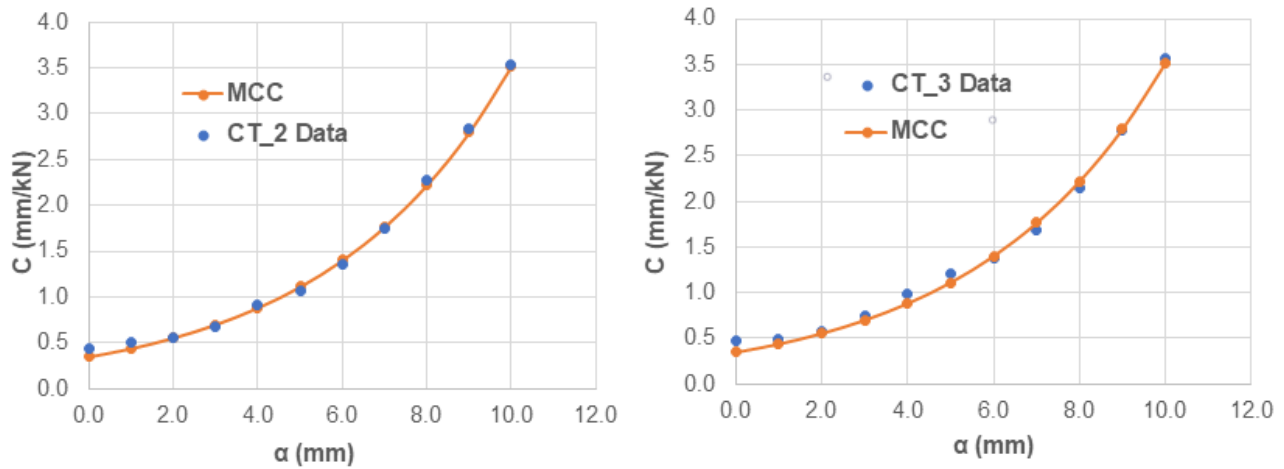


Figure A.12: Modified compliance calibration method graph of specimens CT\_2 and 3.

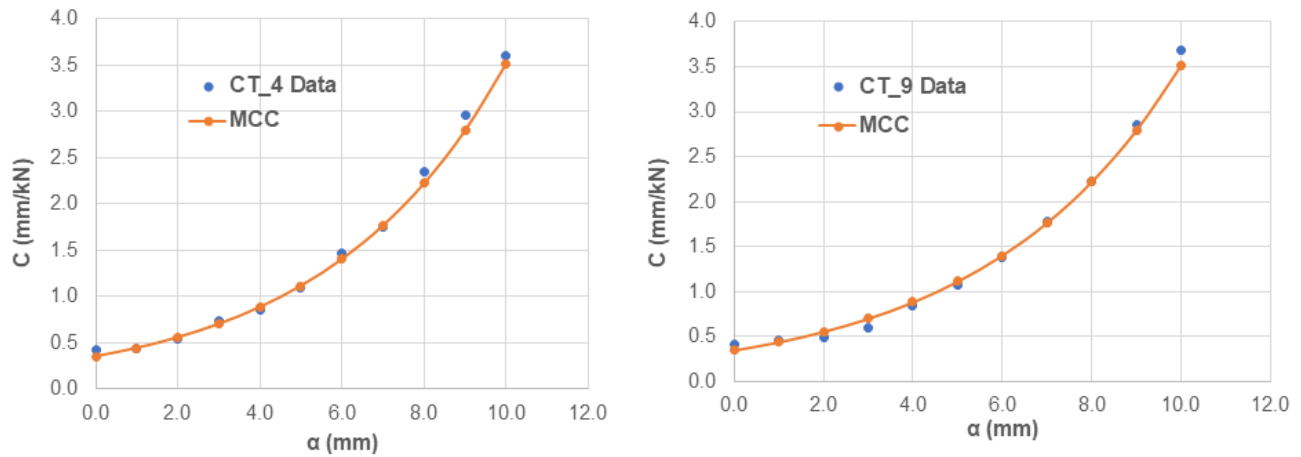


Figure A.13: Modified compliance calibration method graph of specimens CT\_4 and 5.



# Appendix B

## Numerical Analysis Data

### B.1 Material Properties

#### B.1.1 Mechanical properties of the constituents

| $E_{F11}$ (GPa) | $E_{F22}$ (GPa) | $G_{F12}$ (GPa) | $\nu_{F12}$ (-) | $X_{T_F}$ (MPa) | $\rho_F$ (g/cm <sup>3</sup> ) |
|-----------------|-----------------|-----------------|-----------------|-----------------|-------------------------------|
| 230.0           | 22.0            | 20.0            | 0.2             | 4900            | 1.7651                        |

Table B.1: *Mechanical properties for T700S fibres obtained from [1].*

| $E_M$ (GPa) | $G_M$ (GPa) | $\nu_M$ (-) | $X_{T_M}$ (MPa) | $X_{C_M}$ (MPa) | $\rho_M$ (g/cm <sup>3</sup> ) |
|-------------|-------------|-------------|-----------------|-----------------|-------------------------------|
| 2.8         | 1.03        | 0.36        | 80.0            | 116.0           | 1.2028                        |

Table B.2: *Mechanical properties for MTM57 resin system [2].*

| $E_M$ (GPa) | $G_M$ (GPa) | $\nu_M$ (-) | $X_{T_M}$ (MPa) | $X_{C_M}$ (MPa) | $\rho_M$ (g/cm <sup>3</sup> ) |
|-------------|-------------|-------------|-----------------|-----------------|-------------------------------|
| 2.9         | 1.11        | 0.32        | 60.0            | 90.0            | 1.1934                        |

Table B.3: *Mechanical properties for SR8100/SD8824 resin system [3].*

### B.1.2 Mechanical properties at bundle level

|                          | $V_{Fb}$ (%) | $E_{b11}$ (GPa) | $E_{b22}$ (GPa) | $G_{F12}$ (GPa) | $X_{T_b}$ (MPa) | $X_{C_b}$ (MPa) |
|--------------------------|--------------|-----------------|-----------------|-----------------|-----------------|-----------------|
| <b>Tension</b>           | 82.9         | 191.5           | 11.9            | 8.3             | 4100            | 2443            |
| <b>Shear</b>             | 82.7         | 190.7           | 11.1            | 8.25            | 4063            | 2438            |
| <b>Compression</b>       | 82.0         | 189.12          | 10.9            | 8.2             | 4080            | 2417            |
| <b>DCB &amp; 4pt-ENF</b> | 82.5         | 190.2           | 11.0            | 8.17            | 4053            | 2432            |
| <b>CT</b>                | 81.7         | 188.4           | 10.8            | 7.9             | 4014            | 2408            |
| <b>LVI 1</b>             | 79.4         | 182             | 10.1            | 7.24            | 3900            | 2330            |
| <b>LVI 2</b>             | 80.2         | 184.3           | 10.15           | 7.43            | 3930            | 2360            |

Table B.4: *Mechanical properties of fibre bundle based on the fibre volume fraction within a bundle for every loading case.*

### B.1.3 Effective modulus of stitch reinforcement at $\omega = 45^\circ$

The calculation of the effective modulus of the stitches at  $\omega = 45^\circ$  under longitudinal and transverse tension is based on the assumption of an equivalent deflected bar of length  $l$ , cross-sectional area  $A_{Pes}$ , and stiffness  $E_{Pes}$ . Before proceeding to the analytical calculations an assumption was made that the angle  $\omega$  remains constant during the loading scenarios. As illustrated in Fig. 5.3 the green arrowed lines correspond to a longitudinal tensile load,  $T_V$  resulting to an elongation of the stitch equal to  $\epsilon_V$ , whereas the red arrowed lines to transverse tension,  $T_H$  with elongation of the stitch equal to  $\epsilon_H$ .

#### Transverse Tension

#### Longitudinal Tension

The elongation of the stitch yarn due to transverse,  $\epsilon_H$  and longitudinal load,  $\epsilon_V$  can be expressed respectively as:

$$\epsilon_H = \delta_H \cdot \sin \omega$$

$$\epsilon_V = \delta_V \cdot \cos \omega$$

Similarly, the load components in the transverse,  $T_H$  and longitudinal,  $T_V$  direction are obtained:

$$T_H = T \cdot \sin \omega$$

$$T_V = T \cdot \cos \omega$$

Also, with the implementation of Hooke's law for bar deformation,  $T$  for each case is equal to:

$$T = \frac{E_{Pes} \cdot A_{Pes} \cdot \delta_H \cdot \sin \omega}{l}$$

$$T = \frac{E_{Pes} \cdot A_{Pes} \cdot \delta_V \cdot \cos \omega}{l}$$

Hence, by substituting  $T$  with  $T_H / \sin \omega$  and  $T_V / \cos \omega$  respectively, we have:

$$T_H = \frac{E_{Pes} \cdot A_{Pes} \cdot \delta_H \cdot \sin^2 \omega}{l}$$

$$T_V = \frac{E_{Pes} \cdot A_{Pes} \cdot \delta_V \cdot \cos^2 \omega}{l}$$

So, the loads in relation to the effective Young's modulus of the stitches in both directions can be estimated as:

$$T_H = \frac{E_{Pes45^\circ}^{22} \cdot A_{Pes} \cdot \delta_H}{l \cdot \sin \omega}$$

$$T_V = \frac{E_{Pes45^\circ}^{11} \cdot A_{Pes} \cdot \delta_V}{l \cdot \cos \omega}$$

assuming constant  $A_{Pes}$ , we finally get:

$$E_{Pes45^\circ}^{22} = E_{Pes} \cdot \sin^3 \omega$$

$$E_{Pes45^\circ}^{11} = E_{Pes} \cdot \cos^3 \omega$$

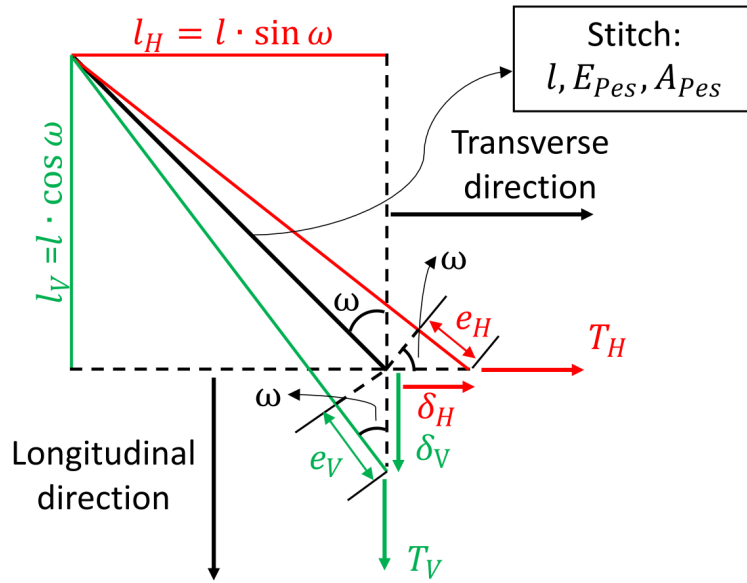


Figure B.1: *Stitch deformation due to longitudinal and transverse tensile loads. (Figure not scaled, for reference only).*

### B.1.4 Mechanical properties at layer level

|                                | Tension | In-plane<br>Shear | Compression         | DCB &<br>4pt-ENF | Compact<br>Tension |
|--------------------------------|---------|-------------------|---------------------|------------------|--------------------|
| $V_{Fb}(\%)$                   | 82.9    | 82.7              | 82.0                | 82.5             | 81.7               |
| $V_b(\%)$                      | 77.0    | 75.5              | 76.3                | 74.0             | 74.7               |
| $V_{Fl}(\%)$                   | 63.8    | 62.4              | 62.5                | 61.0             | 61.0               |
| $V_R(\%)$                      | 34.2    | 35.35             | 35.12               | 34.6             | 36.75              |
| $V_{pes}(\%)$                  | 2.0     | 2.25              | 2.35                | 2.4              | 2.5                |
| $d_{layer}(g/cm^3)$            | 1.5623  | 1.5548            | 1.556               | 1.547            | 1.551              |
| $t_{layer}(mm)$                | 0.136   | 0.137             | 0.125               | 0.128            | 0.139              |
| $E_{11}$ (GPa)                 | 147.8   | 144.7             | 145                 | 141.5            | 141.5              |
| $E_{22}$ (GPa)                 | 6.8     | 6.7               | 6.6                 | 6.5              | 6.5                |
| $G_{12}^{+45^\circ}$ (GPa)     | 4.65    | 4.91              | 4.7                 | 4.7              | 4.7                |
| $G_{12}^{-45^\circ}$ (GPa)     | 4.35    | 4.46              | 4.4                 | 4.4              | 4.4                |
| $\nu_{12}$ (-)                 | 0.2547  | 0.256             | 0.2563              | 0.257            | 0.277              |
| $G_{ult}$ (MPa)                |         |                   | 675                 |                  |                    |
| $G_{13}$ (MPa) [130]           |         |                   | 3700                |                  |                    |
| $\nu_{13}$ (-) [130]           |         |                   | 0.30                |                  |                    |
| $\lambda$ (-)                  |         |                   | 59.15               |                  |                    |
| $\beta(MPa^{-3})$              |         |                   | $3.2 \cdot 10^{-8}$ |                  |                    |
| $X_T$ (MPa)                    | 2620    | 2559              | 2543                | 2500             | 2473               |
| $X_C$ (MPa)                    | 1573    | 1535              | 1526                | 1500             | 1484               |
| $S_O$ (MPa)                    |         |                   | 10                  |                  |                    |
| $Z_T$ (MPa) [130]              |         |                   | 30                  |                  |                    |
| $X_{CF}$ (MPa)                 |         |                   | 64.5                |                  |                    |
| $Y_{CF}$ (MPa)                 |         |                   | 64.5                |                  |                    |
| $G_{ICTInit.}^{0^\circ}(N/mm)$ |         |                   | 83                  |                  |                    |
| $G_{ICTProp.}^{0^\circ}(N/mm)$ |         |                   | 143.5               |                  |                    |
| $G_{ICC}^{0^\circ}(N/mm)$      |         |                   | 80                  |                  |                    |
| $G_{IICT}^{90^\circ}(N/mm)$    |         |                   | 0.6                 |                  |                    |
| $G_{IICC}^{90^\circ}(N/mm)$    |         |                   | 10                  |                  |                    |
| $G_{ICInit.}(N/mm)$            |         |                   | 0.32                |                  |                    |
| $G_{ICProp.}(N/mm)$            |         |                   | 0.71                |                  |                    |
| $G_{IICInit.}(N/mm)$           |         |                   | 0.69                |                  |                    |
| $G_{IICProp.}(N/mm)$           |         |                   | 1.67                |                  |                    |

Table B.6: Material properties of T700S/MTM57 at layer level for each loading case.

|                                     | LVI Laminate RTM_1 | LVI Laminate RTM_2  |
|-------------------------------------|--------------------|---------------------|
| $V_{Fb}(\%)$                        | 79.0               | 79.9                |
| $V_b(\%)$                           | 71.6               | 72.0                |
| $V_{Fl}(\%)$                        | 56.5               | 57.5                |
| $V_R(\%)$                           | 41.51              | 40.23               |
| $V_{pes}(\%)$                       | 2.4                | 2.3                 |
| $d_{layer}(g/cm^3)$                 | 1.5216             | 1.5548              |
| $t_{layer}(mm)$                     | 0.156              | 0.155               |
| $E_{11}$ (GPa)                      | 131.3              | 133.5               |
| $E_{22}$ (GPa)                      | 6.0                | 6.0                 |
| $G_{12}^{+45^{\circ}}(GPa)$         | 4.65               | 4.91                |
| $G_{12}^{-45^{\circ}}(GPa)$         | 4.35               | 4.46                |
| $\nu_{12}$ (-)                      | 0.2547             | 0.256               |
| $G_{ult}$ (MPa)                     |                    | 675                 |
| $G_{13}$ (MPa) [130]                |                    | 3700                |
| $\nu_{13}$ (-) [130]                |                    | 0.30                |
| $\lambda$ (-)                       |                    | 59.15               |
| $\beta(MPa^{-3})$                   |                    | $3.6 \cdot 10^{-8}$ |
| $X_T$ (MPa)                         | 2620               | 2559                |
| $X_C$ (MPa)                         | 1573               | 1535                |
| $S_O$ (MPa)                         |                    | 10                  |
| $Z_T$ (MPa) [130]                   |                    | 30                  |
| $X_{CF}$ (MPa)                      |                    | 64.5                |
| $Y_{CF}$ (MPa)                      |                    | 64.5                |
| $G_{ICT_{Init.}}^{0^{\circ}}(N/mm)$ |                    | 83                  |
| $G_{ICT_{Prop.}}^{0^{\circ}}(N/mm)$ |                    | 143.5               |
| $G_{ICC}^{0^{\circ}}(N/mm)$         |                    | 80                  |
| $G_{ICT}^{90^{\circ}}(N/mm)$        |                    | 0.6                 |
| $G_{ICC}^{90^{\circ}}(N/mm)$        |                    | 10                  |
| $G_{IC_{Init.}}(N/mm)$              |                    | 0.32                |
| $G_{IC_{Prop.}}(N/mm)$              |                    | 0.71                |
| $G_{IIC_{Init.}}(N/mm)$             |                    | 0.69                |
| $G_{IIC_{Prop.}}(N/mm)$             |                    | 1.67                |

Table B.7: Material properties of T700S/SD8100/SR8824 at layer level for LVI cases.

## B.2 Fibre misalignment values for the FE models

### B.2.1 Tension FEA

#### B.2.1.1 In-plane fibre misalignment

| Layers \ Section | A     | B    | C    | D    |
|------------------|-------|------|------|------|
|                  |       |      |      |      |
| Top              | 2.1°  | 2.3° | 2.2° | 2.0° |
| Interior         | 2.05° | 2.2° | 2.2° | 2.2° |
| Bottom           | 2.0°  | 2.1° | 2.2° | 2.4° |

Table B.8: *In-plane fibre misalignment values of the tensile specimen, defined to the top, interior and bottom layers.*

#### B.2.1.2 Out-of-plane fibre misalignment

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 3.2°   | 3.2°   | 3.4°   | 4.0°   | 4.3°   | 3.6°   | 3.3°   | 2.5°   |
| B                 | 3.0°   | 3.6°   | 3.9°   | 3.2°   | 3.8°   | 4.1°   | 3.0°   | 2.6°   |
| C                 | 3.3°   | 3.9°   | 4.1°   | 3.5°   | 3.8°   | 3.5°   | 3.7°   | 2.6°   |
| D                 | 3.6°   | 4.2°   | 3.4°   | 3.2°   | 3.0°   | 3.3°   | 2.8°   | 2.5°   |

Table B.9: *Out-of-plane fibre misalignment values of the tensile specimen, defined per layer and section as observed from the OM.*

### B.2.2 In-plane Shear FEA

#### B.2.2.1 In-plane fibre misalignment

| Layers \ Section | A    | B     | C    |
|------------------|------|-------|------|
|                  |      |       |      |
| Top              | 1.8° | 2.0°  | 1.6° |
| Interior         | 1.7° | 1.95° | 1.8° |
| Bottom           | 1.6° | 1.9°  | 2.0° |

Table B.10: *In-plane fibre misalignment values of the in-plane shear specimen, defined to the top, interior and bottom layers.*

### B.2.2.2 Out-of-plane fibre misalignment

| Section \ Blanket | 1: 45°/-45° | 2: 45°/-45° | 3: 45°/-45° | 4: 45°/-45° |
|-------------------|-------------|-------------|-------------|-------------|
|                   |             |             |             |             |
| A                 | 3.6°        | 3.0°        | 2.6°        | 4.5°        |
| B                 | 3.0°        | 3.7°        | 4.4°        | 3.8°        |
| C                 | 3.4°        | 3.2°        | 3.9°        | 4.0°        |
| Section           | 5: -45°/45° | 6: -45°/45° | 7: -45°/45° | 8: -45°/45° |
|                   |             |             |             |             |
| A                 | 3.6°        | 4.0°        | 3.0°        | 2.2°        |
| B                 | 3.2°        | 4.4°        | 3.3°        | 2.5°        |
| C                 | 3.0°        | 3.1°        | 3.5°        | 2.6°        |

Table B.11: *Out-of-plane fibre misalignment values of the in-plane shear specimen, defined per layer and section as observed from the OM.*

## B.2.3 Compression FEA

### B.2.3.1 In-plane fibre misalignment

| Layers \ Section | A    | B     | C    |
|------------------|------|-------|------|
|                  |      |       |      |
| Top              | 1.2° | 1.8°  | 2.3° |
| Interior         | 1.4° | 1.65° | 2.1° |
| Bottom           | 1.8° | 1.5°  | 1.9° |

Table B.12: *In-plane fibre misalignment of the compression specimen (without deliberate disorientation), defined to the top, interior and bottom layers.*

| Layers \ Section | A     | B    | C    |
|------------------|-------|------|------|
|                  |       |      |      |
| Top              | 3.7°  | 4.2° | 3.9° |
| Interior         | 3.95° | 4.1° | 3.9° |
| Bottom           | 4.2°  | 4.0° | 3.9° |

Table B.13: *In-plane fibre misalignment of the compression specimen (2°nominal fibre disorientation), defined to the top, interior and bottom layers.*

| Layers \ Section | A    | B     | C    |
|------------------|------|-------|------|
|                  |      |       |      |
| Top              | 6.1° | 5.9°  | 6.0° |
| Interior         | 5.8° | 5.85° | 6.1° |
| Bottom           | 5.5° | 5.8°  | 6.2° |

Table B.14: *In-plane fibre misalignment of the compression specimen (4°nominal fibre disorientation), defined to the top, interior and bottom layers.*

| Layers \ Section | A    | B     | C     |
|------------------|------|-------|-------|
|                  |      |       |       |
| Top              | 7.0° | 7.8°  | 7.7°  |
| Interior         | 7.4° | 7.95° | 7.85° |
| Bottom           | 7.8° | 8.1°  | 8.0°  |

Table B.15: *In-plane fibre misalignment of the compression specimen (6° nominal fibre disorientation), defined to the top, interior and bottom layers.*

| Layers \ Section | A    | B     | C     |
|------------------|------|-------|-------|
|                  |      |       |       |
| Top              | 9.2° | 10.2° | 10.0° |
| Interior         | 9.5° | 9.85° | 9.8°  |
| Bottom           | 9.8° | 9.5°  | 9.6°  |

Table B.16: *In-plane fibre misalignment of the compression specimen (8° nominal fibre disorientation), defined to the top, interior and bottom layers.*

| Layers \ Section | A      | B      | C      |
|------------------|--------|--------|--------|
|                  |        |        |        |
| Top              | 11.7°  | 10.4°  | 11.0°  |
| Interior         | 11.85° | 10.55° | 11.65° |
| Bottom           | 12.0°  | 11.5°  | 12.3°  |

Table B.17: *In-plane fibre misalignment of the compression specimen (10° nominal fibre disorientation), defined to the top, interior and bottom layers.*

### B.2.3.2 Out-of-plane fibre misalignment

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 2.0°   | 1.7°   | 3.1°   | 3.4°   | 2.5°   | 2.3°   | 1.8°   | 1.4°   |
| B                 | 2.6°   | 2.0°   | 2.1°   | 1.0°   | 3.1°   | 1.6°   | 2.5°   | 2.9°   |
| C                 | 3.2°   | 1.9°   | 1.8°   | 3.5°   | 2.2°   | 2.2°   | 1.1°   | 3.6°   |

Table B.18: *Out-of-plane fibre misalignment values of the compression specimen (without deliberate disorientation), defined per layer and section as observed from the OM.*

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 2.0°   | 4.5°   | 3.6°   | 3.3°   | 4.3°   | 4.0°   | 1.9°   | 2.2°   |
| B                 | 3.6°   | 2.0°   | 1.8°   | 3.0°   | 2.5°   | 2.1°   | 2.7°   | 1.7°   |
| C                 | 2.0°   | 2.8°   | 3.1°   | 1.9°   | 4.8°   | 2.8°   | 2.9°   | 2.8°   |

Table B.19: *Out-of-plane fibre misalignment values of the compression specimen (2° nominal fibre disorientation), defined per layer and section as observed from the OM.*



| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 3.3°   | 1.1°   | 1.1°   | 3.0°   | 2.7°   | 3.2°   | 2.8°   | 1.2°   |
| B                 | 3.5°   | 2.4°   | 3.2°   | 3.1°   | 2.4°   | 2.2°   | 2.6°   | 2.6°   |
| C                 | 3.4°   | 3.3°   | 1.1°   | 2.6°   | 3.1°   | 2.6°   | 2.8°   | 1.5°   |

Table B.20: *Out-of-plane fibre misalignment values of the compression specimen (4°nominal fibre disorientation), defined per layer and section as observed from the OM.*

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 3.3°   | 2.4°   | 1.7°   | 3.0°   | 3.5°   | 3.2°   | 2.8°   | 3.2°   |
| B                 | 2.5°   | 3.3°   | 3.2°   | 4.0°   | 4.7°   | 2.6°   | 3.4°   | 3.6°   |
| C                 | 4.0°   | 3.4°   | 1.8°   | 2.6°   | 2.4°   | 2.9°   | 3.5°   | 3.5°   |

Table B.21: *Out-of-plane fibre misalignment values of the compression specimen (6°nominal fibre disorientation), defined per layer and section as observed from the OM.*

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 2.2°   | 3.0°   | 2.0°   | 5.5°   | 3.1°   | 3.2°   | 4.3°   | 3.3°   |
| B                 | 4.4°   | 3.1°   | 2.1°   | 4.5°   | 4.0°   | 4.3°   | 4.3°   | 2.4°   |
| C                 | 2.5°   | 5.4°   | 1.9°   | 3.9°   | 4.5°   | 1.8°   | 4.6°   | 3.7°   |

Table B.22: *Out-of-plane fibre misalignment values of the compression specimen (8°nominal fibre disorientation), defined per layer and section as observed from the OM.*

| Section \ Blanket | 1:     | 2:     | 3:     | 4:     | 5:     | 6:     | 7:     | 8:     |
|-------------------|--------|--------|--------|--------|--------|--------|--------|--------|
|                   | 0°/90° | 0°/90° | 0°/90° | 0°/90° | 90°/0° | 90°/0° | 90°/0° | 90°/0° |
| A                 | 2.1°   | 3.8°   | 3.5°   | 3.4°   | 3.3°   | 5.1°   | 3.7°   | 3.8°   |
| B                 | 3.7°   | 4.4°   | 3.9°   | 5.2°   | 4.5°   | 2.5°   | 4.8°   | 4.0°   |
| C                 | 4.0°   | 5.4°   | 2.5°   | 4.8°   | 5.0°   | 4.2°   | 3.5°   | 4.8°   |

Table B.23: *Out-of-plane fibre misalignment values of the compression specimen (10°nominal fibre disorientation), defined per layer and section as observed from the OM.*

| Misalignment \ Case | 0°   | 2°   | 4°   | 6°   | 8°   | 10°   |
|---------------------|------|------|------|------|------|-------|
|                     |      |      |      |      |      |       |
| In-plane            | 1.7° | 4.0° | 5.9° | 7.7° | 9.7° | 11.4° |
| Out-of-plane        | 2.3° | 2.8° | 2.5° | 3.1° | 3.4° | 4.0°  |

Table B.24: *The average values of in- and out-of-plane fibre misalignment of the compression specimens for the different nominal fibre disorientation.*

## B.2.4 Compact Tension FEA

### B.2.4.1 In-plane fibre misalignment

| Layers \ Section | A    | B    | C    | D    | E    | F    | G    |
|------------------|------|------|------|------|------|------|------|
|                  |      |      |      |      |      |      |      |
| Top              | 2.4° | 2.1° | 2.3° | 2.2° | 2.2° | 2.0° | 2.4° |
| Interior         | 2.3° | 2.1° | 2.3° | 2.3° | 2.2° | 2.2° | 2.3° |
| Bottom           | 2.1° | 2.1° | 2.3° | 2.5° | 2.1° | 2.4° | 2.3° |

Table B.25: *In-plane fibre misalignment values of CT specimen, defined to the top, interior and bottom layers.*

### B.2.4.2 Out-of-plane fibre misalignment

| Blanket \ Section | A    | B    | C    | D    | E    | F    | G    |
|-------------------|------|------|------|------|------|------|------|
|                   |      |      |      |      |      |      |      |
| 1: 90°/0°         | 3.8° | 4.7° | 4.4° | 4.6° | 4.6° | 3.5° | 4.7° |
| 2: 90°/0°         | 4.3° | 3.3° | 4.5° | 4.3° | 3.7° | 3.4° | 3.9° |
| 3: 90°/0°         | 4.5° | 4.0° | 3.8° | 3.7° | 4.6° | 3.2° | 4.3° |
| 4: 90°/0°         | 4.5° | 4.7° | 4.0° | 4.7° | 4.3° | 3.4° | 4.8° |
| 5: 90°/0°         | 4.4° | 4.6° | 4.3° | 3.8° | 4.7° | 3.3° | 4.7° |
| 6: 90°/0°         | 4.4° | 4.2° | 4.7° | 4.6° | 3.8° | 4.2° | 4.3° |
| 7: 90°/0°         | 4.8° | 4.6° | 4.1° | 4.1° | 4.3° | 4.2° | 4.0° |
| 8: 90°/0°         | 4.6° | 3.6° | 3.9° | 3.6° | 4.4° | 4.0° | 4.8° |
| 9: 90°/0°         | 4.6° | 3.6° | 3.9° | 3.6° | 4.4° | 4.0° | 4.7° |
| 10: 90°/0°        | 4.3° | 3.9° | 4.7° | 3.4° | 3.6° | 4.1° | 3.4° |
| 11: 90°/0°        | 3.8° | 3.3° | 4.7° | 3.7° | 4.2° | 4.4° | 4.7° |
| 12: 90°/0°        | 4.2° | 4.0° | 4.5° | 4.7° | 4.4° | 4.2° | 3.4° |
| 13: 90°/0°        | 4.2° | 3.4° | 4.1° | 3.3° | 4.3° | 3.4° | 3.8° |
| 14: 90°/0°        | 3.8° | 3.7° | 3.4° | 3.9° | 3.8° | 3.2° | 4.0° |
| 15: 0°/90°        | 3.9° | 4.1° | 4.7° | 4.3° | 4.8° | 4.2° | 4.3° |
| 16: 0°/90°        | 4.0° | 4.6° | 4.2° | 4.4° | 3.6° | 3.5° | 4.0° |
| 17: 0°/90°        | 3.4° | 4.1° | 3.5° | 4.0° | 4.3° | 4.7° | 4.2° |
| 18: 0°/90°        | 3.2° | 3.2° | 3.4° | 4.0° | 4.3° | 3.6° | 3.3° |
| 19: 0°/90°        | 4.2° | 4.4° | 3.5° | 3.9° | 3.8° | 4.8° | 3.4° |
| 20: 0°/90°        | 3.3° | 4.2° | 4.6° | 4.8° | 3.4° | 4.1° | 3.7° |
| 21: 0°/90°        | 3.9° | 3.9° | 4.6° | 4.1° | 4.3° | 4.7° | 3.6° |
| 22: 0°/90°        | 4.6° | 3.3° | 4.2° | 4.6° | 3.6° | 3.5° | 3.5° |
| 23: 0°/90°        | 4.3° | 4.2° | 3.4° | 3.8° | 3.5° | 3.3° | 3.5° |
| 24: 0°/90°        | 3.5° | 4.6° | 3.3° | 3.5° | 3.7° | 4.4° | 4.2° |
| 25: 0°/90°        | 4.4° | 4.5° | 3.8° | 4.5° | 4.3° | 4.1° | 3.9° |
| 26: 0°/90°        | 4.7° | 4.4° | 4.6° | 4.6° | 3.5° | 4.7° | 4.3° |
| 27: 0°/90°        | 4.3° | 3.3° | 4.5° | 4.3° | 3.7° | 3.4° | 3.9° |
| 28: 0°/90°        | 3.7° | 4.2° | 3.4° | 4.6° | 3.4° | 3.8° | 3.3° |

Table B.26: *Out-of-plane fibre misalignment values of CT specimen, defined per layer and section as observed from the OM.*

## B.2.5 Low Velocity Impact (Case A) FEA

### B.2.5.1 In-plane fibre misalignment

| Section<br>Layers           | A                    | B                    | C                     | D                    | E                     | F                    | G                    | H                    | I                    |
|-----------------------------|----------------------|----------------------|-----------------------|----------------------|-----------------------|----------------------|----------------------|----------------------|----------------------|
| Top                         | 1.2°                 | 1.3°                 | 1.2°                  | 1.4°                 | 1.8°                  | 1.5°                 | 1.6°                 | 1.6°                 | 1.3°                 |
| Interior                    | 1.3°                 | 1.3°                 | 1.25°                 | 1.3°                 | 1.65°                 | 1.7°                 | 1.7°                 | 1.7°                 | 1.5°                 |
| Interior (#13,<br>#14, #15) | $1.3^\circ + \phi_I$ | $1.3^\circ + \phi_I$ | $1.25^\circ + \phi_I$ | $1.3^\circ + \phi_I$ | $1.65^\circ + \phi_I$ | $1.7^\circ + \phi_I$ | $1.7^\circ + \phi_I$ | $1.7^\circ + \phi_I$ | $1.5^\circ + \phi_I$ |
| Bottom                      | 1.4°                 | 1.3°                 | 1.3°                  | 1.2°                 | 1.5°                  | 1.9°                 | 1.8°                 | 1.8°                 | 1.7°                 |

Table B.27: *In-plane fibre misalignment values of RTM-1 LVI specimen, defined to the top, interior and bottom layers.*

\*For the NCF blankets #13, #14 and #15, which are deliberately misaligned, the angular term  $\phi_I$  is added which corresponds to 0°, 4° and 8° of in-plane misorientation.

### B.2.5.2 Out-of-plane fibre misalignment

| Section<br>Blanket | A     | B     | C     | D     | E     | F     | G     | H     | I     |
|--------------------|-------|-------|-------|-------|-------|-------|-------|-------|-------|
| 1: 90°/0°          | 2.25° | 3.75° | 3°    | 1.5°  | 3.75° | 2.25° | 2.25° | 1.5°  | 2.25° |
| 2: 90°/0°          | 2.25° | 3°    | 2.25° | 2.25° | 3.75° | 1.5°  | 3.75° | 1.5°  | 2.25° |
| 3: 90°/0°          | 3.75° | 3.75° | 2.25° | 2.25° | 2.25° | 1.5°  | 3.75° | 1.5°  | 2.25° |
| 4: 90°/0°          | 3°    | 1.5°  | 1.5°  | 3.75° | 3°    | 2.25° | 1.5°  | 3.75° | 2.25° |
| 5: 90°/0°          | 3°    | 1.5°  | 3°    | 3.75° | 3.75° | 1.5°  | 2.25° | 1.5°  | 3°    |
| 6: 90°/0°          | 3°    | 3.75° | 1.5°  | 1.5°  | 3.75° | 2.25° | 3.75° | 3.75° | 1.5°  |
| 7: 90°/0°          | 3°    | 2.25° | 1.5°  | 3°    | 3°    | 3.75° | 1.5°  | 2.25° | 1.5°  |
| 8: 90°/0°          | 3.75° | 3°    | 3°    | 1.5°  | 3°    | 3.75° | 1.5°  | 2.25° | 1.5°  |
| 9: 90°/0°          | 1.5°  | 2.25° | 2.25° | 3.75° | 3.75° | 2.25° | 2.25° | 3.75° | 3°    |
| 10: 90°/0°         | 3°    | 3.75° | 3°    | 2.25° | 1.5°  | 2.25° | 1.5°  | 2.25° | 2.25° |
| 11: 90°/0°         | 2.25° | 2.25° | 3°    | 3.75° | 1.5°  | 3°    | 3.75° | 1.5°  | 2.25° |
| 12: 90°/0°         | 1.5°  | 3°    | 3°    | 2.25° | 1.5°  | 2.25° | 1.5°  | 1.5°  | 3.75° |
| 13: 90°/0°         | 2.25° | 1.5°  | 3.75° | 1.5°  | 3.75° | 1.5°  | 2.25° | 3°    | 1.5°  |
| 14: 90°/0°         | 3°    | 1.5°  | 3.75° | 3°    | 3.75° | 3.75° | 2.25° | 2.25° | 3°    |
| 15: 0°/90°         | 3.75° | 1.5°  | 3.75° | 1.5°  | 3°    | 2.25° | 1.5°  | 3°    | 3°    |
| 16: 0°/90°         | 3.75° | 2.25° | 3.75° | 2.25° | 3.75° | 2.25° | 1.5°  | 3.75° | 3.75° |

Table B.28: *Out-of-plane fibre misalignment values of RTM-1 LVI specimen, defined per layer and section as observed from the OM.*

## B.2.6 Low Velocity Impact (Case B) FEA

### B.2.6.1 In-plane fibre misalignment

| Section<br>Layers | A    | B     | C    | D     | E     | F    | G     | H    | I     |
|-------------------|------|-------|------|-------|-------|------|-------|------|-------|
| Top               | 1.3° | 1.3°  | 1.4° | 1.5°  | 1.5°  | 1.3° | 1.8°  | 1.5° | 1.4°  |
| Interior          | 1.3° | 1.35° | 1.4° | 1.45° | 1.35° | 1.4° | 1.65° | 1.6° | 1.55° |
| Bottom            | 1.3° | 1.4°  | 1.4° | 1.4°  | 1.2°  | 1.5° | 1.5°  | 1.7° | 1.7°  |

Table B.29: *In-plane fibre misalignment values of RTM-2 LVI specimen, defined to the top, interior and bottom layers.*

### B.2.6.2 Out-of-plane fibre misalignment

| Section<br>Blanket | A     | B     | C     | D     | E     | F     | G     | H     | I     |
|--------------------|-------|-------|-------|-------|-------|-------|-------|-------|-------|
| 1: 90°/0°          | 1.44° | 2.88° | 1.44° | 2.16° | 2.16° | 3.6°  | 3.6°  | 2.88° | 2.88° |
| 2: 90°/0°          | 2.16° | 2.16° | 2.88° | 3.6°  | 1.44° | 2.16° | 2.88° | 2.16° | 2.16° |
| 3: 90°/0°          | 3.6°  | 1.44° | 2.7°  | 2.35° | 3.6°  | 2.9°  | 2.4°  | 2.4°  | 2.3°  |
| 4: 90°/0°          | 1.44° | 2.88° | 2.88° | 1.44° | 2.88° | 3.6°  | 2.88° | 2.88° | 3.6°  |
| 5: 90°/0°          | 2.16° | 2.88° | 3.6°  | 1.44° | 2.88° | 2.16° | 1.44° | 2.16° | 2.88° |
| 6: 90°/0°          | 3.6°  | 3.6°  | 1.44° | 2.16° | 1.44° | 2.88° | 2.88° | 1.44° | 2.88° |
| 7: 90°/0°          | 2.16° | 1.44° | 2.88° | 2.88° | 1.44° | 1.44° | 1.44° | 3.6°  | 3.6°  |
| 8: 90°/0°          | 2.88° | 3.6°  | 2.16° | 3.6°  | 2.16° | 1.44° | 2.88° | 3.6°  | 2.88° |
| 9: 90°/0°          | 2.16° | 1.44° | 2.16° | 3.6°  | 2.88° | 1.44° | 2.88° | 2.16° | 1.44° |
| 10: 90°/0°         | 1.44° | 2.88° | 2.88° | 2.88° | 2.16° | 3.6°  | 2.16° | 2.88° | 3.6°  |
| 11: 90°/0°         | 2.16° | 2.88° | 1.44° | 2.16° | 3.6°  | 3.6°  | 2.88° | 3.6°  | 2.88° |
| 12: 90°/0°         | 3.6°  | 2.88° | 2.88° | 2.88° | 3.6°  | 1.44° | 1.44° | 3.6°  | 2.16° |
| 13: 90°/0°         | 1.44° | 2.88° | 2.16° | 3.6°  | 3.6°  | 3.6°  | 2.16° | 2.88° | 3.6°  |
| 14: 90°/0°         | 2.88° | 3.6°  | 2.88° | 2.16° | 1.44° | 3.6°  | 3.6°  | 2.16° | 2.4°  |
| 15: 0°/90°         | 3.6°  | 2.16° | 3.6°  | 2.16° | 3.6°  | 2.88° | 1.44° | 2.16° | 2.6°  |
| 16: 0°/90°         | 3.6°  | 2.88° | 1.44° | 3.6°  | 2.88° | 2.88° | 3.6°  | 2.88° | 3.4°  |

Table B.30: *Out-of-plane fibre misalignment values of RTM-2 LVI specimen, defined per layer and section as observed from the OM.*