

**Experimental Characterization and Numerical Modelling of the Energy Absorption Capacity of
UD-NCF Carbon Fibre/Epoxy Composite Channels**

by

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Author's Declaration

I hereby declare that I am the sole author of this thesis. This is a true copy of the thesis, including any required final revisions, as accepted by my examiners.

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Abstract

While the potential of carbon fibre-reinforced plastic (CFRP) composites for energy-absorbing structures has been widely acknowledged, there has been limited adoption in the automotive industry due to high manufacturing costs and challenges associated with accurately predicting their response during impact loading. Components manufactured via high-pressure resin transfer (HP-RTM) with highly reactive resins offer reduced cycle times, while unidirectional non-crimp fabric (UD-NCF) reinforcements provide a reduction in manufacturing costs, high in-plane mechanical properties, and design flexibility. However, the energy absorption capacity (EAC) of UD-NCF CFRP composites has not been widely studied, with much of the available research focused on the quasi-static axial crushing or dynamic drop-testing of unidirectional tape or woven CFRP composite tubes. Additionally, since predictive modelling is extensively used in the automotive industry, additional research on accurately modelling the EAC of CFRP composite components is required to expand their implementation in vehicle structures. Thus, the goal of this study is to support the development of a high-fidelity impact simulation model for predicting the EAC of UD-NCF carbon fibre-reinforced epoxy composite structures manufactured via HP-RTM.

Firstly, the EAC and failure modes of $[0/\pm45/90]_s$, $[0/90/\pm45]_s$, and $[\pm45/0_2]_s$ tapered hat channels subject to dynamic axial compressive loading were experimentally characterized. Distinct modes of failure were observed for the tested channels, with the highest energy absorption coinciding with a splaying failure mode for the $[\pm45/0_2]_s$ channels (24% to 29% higher than other layups). These results were compared to results from previously performed tests on single corrugated and hat channels comprising the same material system and tested under the same conditions. While the stacking sequence is influential for a given component geometry, manufacturing-induced defects for components with complex geometry can cause significant reductions in EAC. For all geometries, the $[\pm45/0_2]_s$ channels performed best, while the tapered hat channel exhibited up to 25% lower EAC compared to the hat and corrugated channels for this stacking sequence.

Next, simulation models were developed using the finite element software LS-DYNA to predict EAC of the tapered, hat, and corrugated CFRP composite channels with different stacking sequences. Various loading conditions were simulated, including dynamic and quasi-static axial compressive loading and three-point bending. An available material model for the progressive failure of composite laminates, MAT_054, was calibrated for a baseline case, namely the $[0/\pm45/90]_s$ hat

channel under dynamic axial compressive loading. It was concluded that while the EAC of channels with different geometries from the baseline condition was accurately predicted when using the calibrated material model, the predictive capability of the simulation models was limited for other stacking sequences and loading rates and for the three-point bending cases. The calibrated material model was unable to capture the strain rate-dependent response of the UD-NCF composite material, which resulted in underprediction of EAC at loading rates different from the baseline condition (i.e. 33% for the $[0/\pm 45/90]_s$ hat channel subject to quasi-static axial loading). Additionally, the single-shell part representation used in the study prevented capture of delamination, which resulted in underpredictions for energy absorption when delamination was prominent (i.e. 24% for $[\pm 45/0_2]_s$ hat channels subjected to dynamic axial loading). Lastly, the EAC was significantly overpredicted for the three-point bending cases (from 13% for the $[\pm 45/0_2]_s$ corrugated channels up to 60% for $[\pm 45/0_2]_s$ hat channels subject to dynamic three-point bending) owing to the fact that some of the material model parameters were non-physical and thus tailored for the axial compression loading case. In general, changes in stacking sequence and loading condition required recalibration of the non-physical material model parameters; most notably, the mode-dependent failure strains.

Lastly, the energy-absorption capacity and failure behaviour of adhesively bonded double hat channels under axial compressive loading were experimentally characterized and corresponding simulation models were developed with the calibrated material model. The average total energy absorption was higher for the $[\pm 45/0_2]_s$ channels when compared to the $[0/\pm 45/90]_s$ channels with a 7.5% and 32% increase for quasi-static and dynamic cases, respectively. However, the average specific energy absorption of all double-hat channels tested was less than that measured for the single-hat channel counterpart of the same stacking sequence for both loading rates. To represent the bond numerically, the elastoplastic rate-dependent material model, MAT_240, was calibrated in LS_DYNA to experimental data and applied to cohesive elements. Predictions of EAC were more agreeable with experimental data for the dynamic axial loading cases (deviations of +11% and -21% for the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ channels respectively) than the quasi-static axial loadings (deviations of -33% and -35% for the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ channels respectively).

The study yielded critical performance data for UD-NCF CFRP channels and an improved understanding of the associated influence of stacking sequence, component geometry, and loading rate. The simulation results serve as a benchmark to which more physically based models (i.e., considering interlaminar interactions, strain rate dependencies, etc.) can be confidently compared.

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Chapter 1

Introduction

1.1 Research Motivation

In 2021, transportation accounted for 28% of Canada's greenhouse gas emissions, the majority of which was due to road transportation (i.e., passenger and freight vehicles) [1]. Recent federal zero-emission targets for new passenger vehicles and light trucks have motivated automotive manufacturers to improve vehicle design and production processes, such that emissions from road transportation are reduced [2], [3]. The replacement of traditionally heavy metal components with advanced, lighter materials (known as light-weighting) is one method by which increasing fuel economy restrictions may be addressed without sacrificing vehicle performance [4]. A 10% reduction in weight can translate to a 6% to 8% improvement in fuel economy [5]. Weight reduction is not only important in improving fuel efficiency but also in compensating for the additional weight introduced by electric batteries in electric-powered vehicles [4]. Registrations of gas-powered vehicles dropped from 88.0% in 2020 to 80.6% in 2022, while the number of newly registered hybrid electric vehicles, plug-in hybrid electric vehicles, and battery electric vehicles more than doubled to 14.7% [6], indicating increased interest in low-emission vehicles.

Fibre-reinforced plastic (FRP) composite materials are viable light-weighting options for vehicle structures as they offer several advantages when compared to their metallic counterparts, such as high specific strength and stiffness, increased design flexibility, reduced maintenance (no corrosion), and reduced assembly owing to part consolidation [7]. As a result, carbon fibre monocoques have been manufactured to reduce the weight of Formula 1 and luxury vehicles since the 1980s [8]. In 2005, Lamborghini developed an entire carbon fibre/epoxy body (bumper subsystem, fenders, hood, structural frames, etc.) for the Murcielago (Figure 1.1), which yielded a weight reduction of 34 kg (about 40%) over its all-aluminum counterpart, the Diablo [9]. Furthermore, in 2013, BMW launched their first electrically powered production car, the Megacity Vehicle, which featured carbon fibre reinforced plastic (CFRP) composites to reduce the overall mass of the vehicle (Figure 1.2a). A 100 kg battery was introduced in the front of the vehicle, while an equal reduction in body weight was also achieved using a CFRP passenger cell [10].

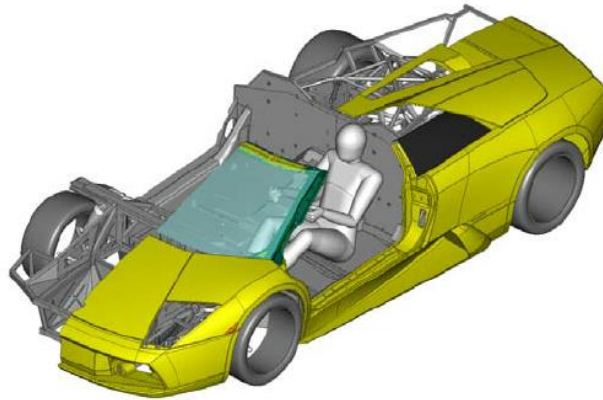


Figure 1.1: CFRP body panels (in lime green) adhesively bonded to the hybrid CFRP/tubular steel frame and roof structure of the Lamborghini Murciélago Roadster [11].

While interest in the application of composite materials for the mass production of automobiles also emerged, it was almost exclusively limited to discontinuous fibre-reinforced sheet compound (SMC) or bulk compound (BMC) for non-structural or secondary parts [12], [13], [8]. For energy-absorbing and structural applications, research has been ongoing since the late 1980s [14]. More specifically, FRP composite structures are known to exhibit high specific energy absorption capabilities under compression and high-speed impact [7]. This property favours adoption for deceleration and occupant protection applications like front crush rails, which are currently made of steel and aluminum. Automotive manufacturers such as FIAT [15], Hypercar Inc. [16], and Ford [17] have all previously developed prototypes to investigate the use of carbon fibre reinforced plastic (CFRP) composites as primary structures. However, this requires the use of stronger, continuous fibre reinforcements (such as unidirectional preregs), which were costly and time-consuming to manufacture [18], [19]. This limited most adoption to luxury and sports cars [20], [21], [22]. Despite their clear advantages, limited adoption of CFRP composites in frontal crash structures has remained as a result of their costly manufacturing processes and complicated energy-absorbing mechanisms.

Fortunately, relatively recent advancements in lower-cost, large-scale FRP manufacturing methods (e.g., liquid compression with heavy-tow fabrics) as well as developments in rapid curing resin systems may enable wider adoption of CFRPs in vehicle structures. From 2019 to 2022, the National Research Council of Canada's Automotive and Surface Transportation Research Centre engaged in an academic and industrially collaborative project to create fast and affordable processes

for manufacturing advanced thermoset composites for vehicle parts. Achieving cycle times of less than three minutes, via a process known as resin transfer (RTM), a part for coach buses 35% lighter than the original metallic part was created and is undergoing tests at an industrial facility [4]. In addition to the reduced manufacturing cycle times [7], developments in fabric architecture for medium-to-high-volume applications are expanding. Non-crimp fabrics (NCFs), for example, feature advantages such as strength, ease of handling, and low manufacturing costs. For the European project, TECABS, which validated the production of lightweight carbon fibre automotive body-in-white structures, RTM in combination with new resins enabled the production of up to 50,000 NCF floor panels per year [8]. Early applications of NCFs in batch production were also accomplished via BMW AG in Munich, Germany, where roof rails were produced [8]. BMW continues to use NCFs manufactured via HP-RTM in their vehicles (Figure 1.2b) [10]. Thus, the development of frontal crush structures using NCF reinforcements may also improve the accessibility of CFRPs within the automotive industry.



Figure 1.2: (a) Preforming the CFRP roof of the BMW M3 (b) Removal of the BMW M3 roof from a RTM press [10].

As for their complicated energy-absorbing mechanisms, the anisotropic nature of FRPs results in a complex combination of damage mechanisms (i.e., matrix cracking, fibre/matrix debonding, fibre kinking or fracture, delamination and friction [23]) that although efficiently dissipate energy, are also difficult to predict and quantify [14]. The interaction of these mechanisms results in a range of component-level failure modes that can improve or hinder the energy absorption capacity of a part [24]. Furthermore, performance is heavily dependent on part geometry, fibre type and form,

laminate stacking sequence, loading rate, and damage initiation strategies [25]. For example, the energy absorption capacity of CFRPs is often notably reduced under dynamic conditions than at quasi-static rates [26], [27], [28], [25] [29], [30]. However, much of the available literature on the energy absorption properties of CFRPs is collected at quasi-static rates due to the accessibility of quasi-static experiments and therefore cannot be used to guide design for vehicle crash applications. As a result, the design of crash-worthy CFRP structures currently requires extensive physical testing, which is often costly, even on a reduced scale or when focused on specific components. Additionally, in the automotive industry, vehicle performance is often investigated via full-scale numerical structural analysis prior to experimental testing [5]. Several commercial finite element software packages are available for predicting the energy absorption capabilities of CFRP composite structures, including LS-DYNA, PAM-CRASH, Radioss, and Abaqus Explicit, among others [31], [32]. Significant effort has been undertaken to develop material models that can capture the details of composite materials without requiring unreasonable computational resources [31], [33], [34], [35], [36]. Success in the former often comes at the cost of achieving the latter, and vice versa. Towards this goal, a number of progressive failure models that predict the sudden failure of composite plies, such as MAT_054 within LS-DYNA, have been developed and require the calibration of many parameters that are not physically representative [37], [38], [39], [40]. Consequently, the transferability of such models is uncertain, and their use as design tools is limited. Thus, beyond reducing the dependence on experimental testing, accelerating adoption within the automotive industry, specifically, requires the development of efficient numerical techniques and simulation tools that reliably incorporate composite material design and manufacturing processes to predict the performance of CFRP crash-worthy structures. Therefore, the goal of this thesis is to support the development of a high-fidelity impact simulation model for predicting the energy absorption capacity of unidirectional non-crimp fabric (UD-NCF) carbon fibre-reinforced epoxy crash-worthy composite structures.

1.2 Thesis Outline

This thesis is divided into six subsequent chapters through which the experimental characterization of UD-NCF carbon fibre/epoxy composite channels and the development of simulation models to capture and predict experimentally observed behaviour are described and discussed. In Chapter 2, a brief background on CFRP composites is presented, followed by a

summary of studies experimentally investigating the response of CFRPs subject to axial loading and three-point bending. Thereafter, background and relevant studies on numerically modelling CFRPs composites under the same loading conditions are summarized. Lastly, approaches to modelling adhesively bonded composites are provided and followed by relevant studies. A summary of gaps amongst current literature and a set of objectives to support the overall goal of this study are then detailed. In Chapter 3, the manufacture of the UD-NCF CFRP composite channels, as well as the experimental methodology for their characterization under axial loading, is presented. In Chapter 4, the numerical methodology for the development of simulation models to replicate the experimental conditions for calibration and verification is described. Results of the experimental characterization and the simulation model predictions are presented in Chapter 5 and Chapter 6, respectively. Discussion on each set of results is also provided within the respective chapters. Lastly, in Chapter 7, the outcomes of this study are summarized, and recommendations for future work are suggested.

Chapter 2

Background & Literature Review

In this chapter, an overview of fibre-reinforced composite materials is provided, namely the common constituents for energy-absorbing structures, carbon-fibre thermoset composites (Section 2.1). This is followed by a summary of common fabrication processes (Section 2.2). Next, the deformation response of UD composites and laminates is presented, followed by a review of damage mechanisms and failure theories (Section 2.3). Literature on the energy-absorption capacity (EAC) of thermoset composites under axial compressive loading and transverse bending is then presented (Section 2.4). Lastly, literature on simulating the behaviour of composite structures and adhesively bonded composite parts to predict performance is presented (Section 2.5 and Section 2.6). The chapter concludes with a summary of gaps in the reviewed literature and research objectives (Section 2.7).

2.1 Overview of Fibre-Reinforced Composite Materials

Fibre-reinforced plastic composite materials consist of reinforcement fibres embedded in a polymeric matrix. The reinforcement fibres have a much greater strength and stiffness when compared to the matrix phase. The properties of the composite are dependent on the properties of the constituent materials, as well as their concentrations and forms, the orientation of the fibres, and the strength of the fibre-matrix interface [41], [42].

2.1.1 Fibrous Reinforcements

The concentration of the reinforcing fibre phase is measured in terms of volume or weight fraction (V_f , w_f). The orientation of the fibres affects the anisotropy of the material system [41]. Composites comprising continuous fibres exhibit a higher load-bearing capacity when compared to those with discontinuous fibres, while also offering improved impact resistance [42].

Continuous fibre tows (i.e., bundles of fibres) may be used to directly manufacture composite parts or can be formed into fabrics that may be subsequently pre-impregnated with resin to form partially cured prepreg materials [41]. Two-dimensional fabrics are commonly employed because they allow for effective tailoring of anisotropic material properties. There are many types of two-dimensional fabrics used as reinforcements such as unidirectional non-crimp fabrics, woven fabrics

(e.g., plain weave or twill weave), or braided fabrics (e.g., biaxial, triaxial, or quadriaxial) [43], [24] (Figure 2.1). For the composite components assessed in this study, UD-NCFs are used as the reinforcement constituent.

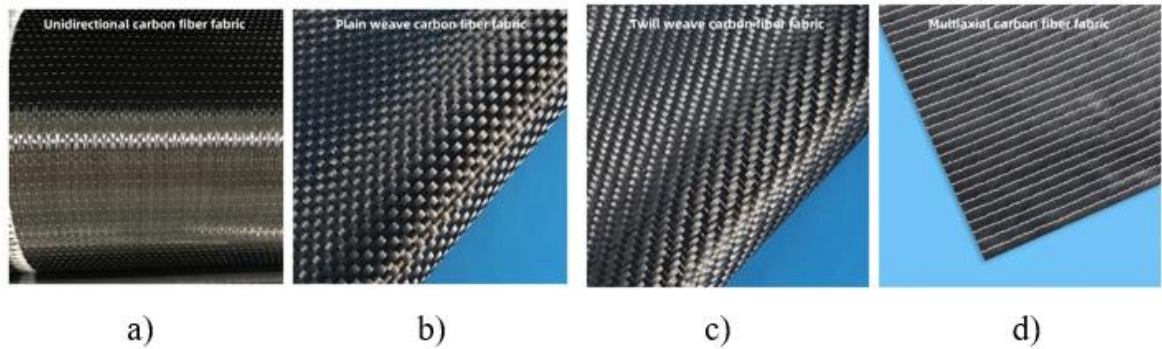


Figure 2.1: Examples of two-dimensional carbon fibre fabrics: a) UD-NCF, b) plain weave, c) twill weave, and d) multidirectional NCF [44].

UD-NCFs consist of parallel fibre tows aligned in one direction, stitched together with a low-density polyester yarn [45] (Figure 2.2a). Since UD-NCFs do not have interlacing fibre tows, they are characterized by having limited out-of-plane crimping [43]. Common stitching patterns include chain, tricot, plain and satin, stitching [46] (Figure 2.2b). Transverse supporting fibres placed below the tows are often used to maintain fabric integrity during handling, while a stabilizing binder is often applied to the fabric to facilitate preforming for liquid composite processes [47]. More recently, cost-effective heavy-tow fabrics with 50,000 filaments per tow have been developed and used for automotive applications [8], [38], [48], [49].

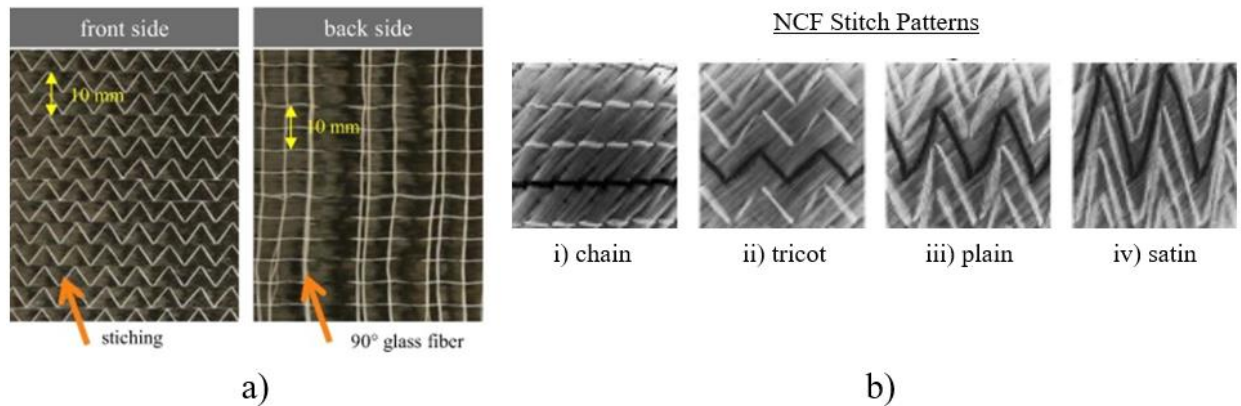


Figure 2.2: a) Example of a carbon fibre unidirectional non-crimp fabric, adapted from [50]. b) Examples of non-crimp fabric stitching patterns, adapted from [46].

2.1.2 Polymeric Matrices

The role of the matrix phase is to bind the fibres, transfer loads to the fibres, and protect them against environmental degradation or damage during handling[41]. Polymers are the most widely used matrix materials due to their low cost, easy processability, and good chemical resistance [41]. However, polymeric matrices are limited to low operating temperatures, which in turn limits the operating temperature of the composite material. Polymer matrix materials include thermosets and thermoplastics, with thermosets being most widely used [24].

Thermoset polymers are characterized by short molecular chains and, thus, are liquids in the as-received condition at room temperature. To be used as the matrix phase in composite materials, thermosets must be combined with a curing agent to form crosslinks between molecules. The curing process forms a molecular network structure, which solidifies thermosets and leads to the development of their characteristic properties. Epoxies are the most common thermosets used in composite materials owing to their relatively high strength and modulus, and low shrinkage during manufacturing [24]. Additionally, epoxies possess strain-rate dependent properties, with demonstrated increases in compressive yield strength with increasing strain rate [51] and transitions from ductile to brittle responses under shear loading [52]. Lastly, highly reactive, fast-curing epoxies have been developed to reduce production cycle times [53], [54] and were used to fabricate the channels considered in this study.

2.2 Fabrication of Fibre-Reinforced Plastic Composite Materials

There are many processes available for the fabrication of FRP composites, including processes utilizing semi-finished raw materials, such as prepregs, and liquid composite processes. Liquid composite processes such as resin transfer (RTM) and wet compression involve impregnating dry fabric reinforcement layers with a liquid resin (typically a thermoset polymer) inside a closed heated mould. These processes offer higher efficiency, flexibility and low cost for complex structures [55]. A variant of the RTM process, namely high-pressure resin transfer (HP-RTM), was used for the fabrication of composite components in this study and will be discussed hereafter.

For a typical RTM process, fabric layers are cut, stacked, and preformed into a near-net shape of the final part before being placed into a two-part preheated mould. Once the mould is closed, a vacuum is used to draw air out of the mould cavity for the purpose of mitigating void formation during subsequent infusion of the resin. Resin is premixed with a curing agent and infused into the mould using a metering unit. Once the mould is filled, the resin is allowed to cure before the solidified part is demoulded. RTM enables the repeatable production of small- to medium-sized, geometrically complex, high-quality continuous FRP composite parts in relatively short cycle times [41] (hours or minutes, depending on the part geometry and thickness [45]). However, the RTM process also features disadvantages such as high initial setup costs and limits to the size of the parts. High-pressure resin transfer was developed to address the limitations of typical resin transfer processes. The HP-RTM process features a highly reactive resin and a curing agent that are rapidly mixed and injected into the closed mould at high flow rates using a metering unit with a mix head. The process is often automated, leading to reduced cycle times of only minutes [56], conducive to high-volume production environments. The high pressure generated in the mould cavity also improves surface finish quality [57].

2.3 Deformation Response and Failure Characteristics of FRP Laminates

Unidirectional composites often represent a basic building block for the construction of FRP laminates comprising multiple layers [41]. Each layer may be referred to as a ply or lamina. The material axes are defined by the direction parallel to the fibres (longitudinal axis, 1), and the directions perpendicular to the fibres (transverse direction, 2, and out-of-plane transverse direction, 3) (Figure 2.3). Unidirectional composites are treated as transversely isotropic materials, where the

material is isotropic in the 2-3 plane. The generalized stress-strain response of an orthotropic FRP lamina can be segregated into four stages: linear-elastic, pre-peak softening, peak, and post-peak softening (Figure 2.4). Pre-peak softening represents the inelastic response of composites attributed to the accumulation of micro-damage and/or localized yielding of the matrix material [31]. The peak represents the initiation of intralaminar failure, which can be predicted using failure criteria (Section 2.3.3). Post-peak softening represents macroscale damage evolution leading to the final failure of a lamina [31].

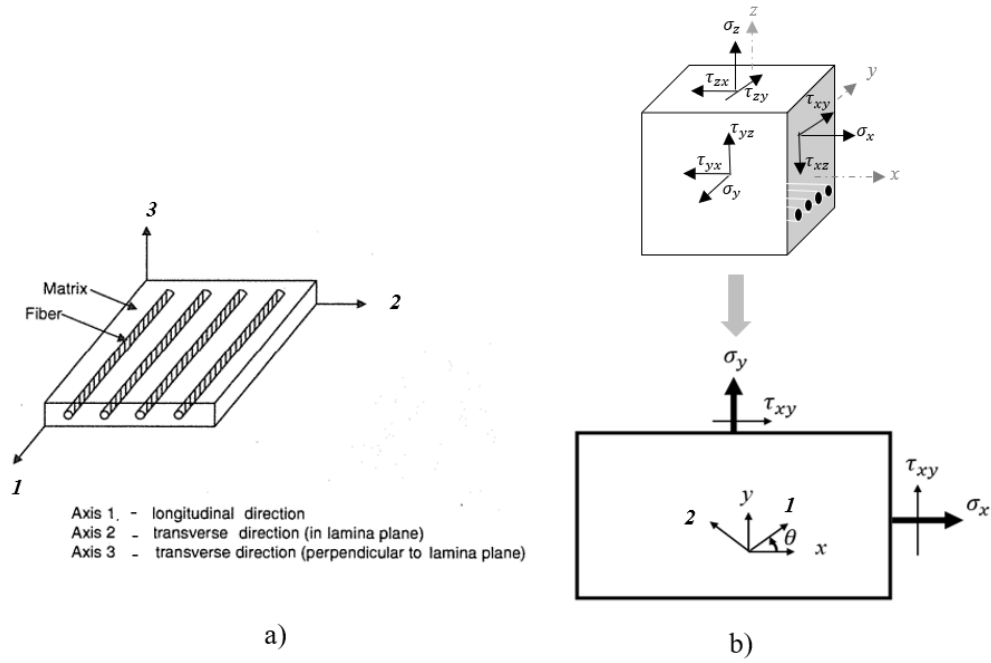


Figure 2.3: Schematic representations of (a) a unidirectional composite and its material coordinate systems, adapted from [41] and (b) Stresses acting on a composite volume element in a state of plane stress.

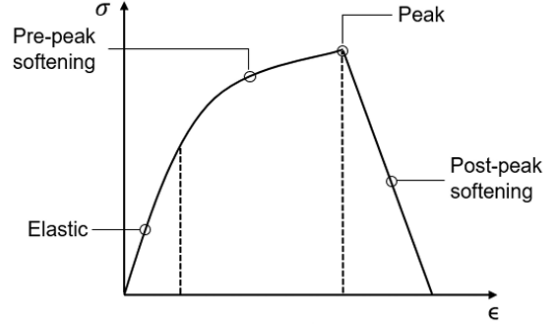


Figure 2.4: Representative stress-strain behaviour of a fibre-reinforced plastic composite [31].

2.3.1 Plane Stress Linear Elastic Response of a Lamina and Classical Lamination Theory

For a lamina in plane stress, the resulting stress-strain relations with respect to the material coordinate system are given by [41]:

$$\begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \tau_{12} \end{Bmatrix} = \begin{bmatrix} Q_{11} & Q_{12} & 0 \\ Q_{12} & Q_{22} & 0 \\ 0 & 0 & Q_{66} \end{bmatrix} \begin{Bmatrix} \epsilon_1 \\ \epsilon_2 \\ \gamma_{12} \end{Bmatrix} \quad (1)$$

where $\{\sigma\}$ and $\{\epsilon\}$ represent the lamina stresses and strains, respectively, and $[Q]$ is the reduced stiffness matrix. The stress-strain relations can also be presented in terms of the compliance matrix, $[S]$, where $[S] = [Q]^{-1}$:

$$\begin{Bmatrix} \epsilon_1 \\ \epsilon_2 \\ \gamma_{12} \end{Bmatrix} = \begin{bmatrix} S_{11} & S_{12} & 0 \\ S_{12} & S_{22} & 0 \\ 0 & 0 & S_{66} \end{bmatrix} \begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \tau_{12} \end{Bmatrix} \quad (2)$$

$$S_{11} = \frac{1}{E_1}, \quad S_{12} = -S_{11}\nu_{12}, \quad S_{22} = \frac{1}{E_2}, \quad S_{66} = \frac{1}{G_{12}} \quad (3)$$

The four elastic constants of the lamina consist of Young's moduli in the longitudinal and transverse directions (E_1 and E_2), the in-plane Poisson's ratio (ν_{12}), and the in-plane shear modulus (G_{12}). For the analysis of a laminate, it is necessary to define a common reference coordinate system (x, y, z) about which each lamina is oriented by angle θ (Figure 2.3b). Stresses and strains can be transformed from one coordinate system to another via the stress-transformation matrix ($[T_1]$) or the strain-

transformation matrix ($[T_2]$)[41]:

$$\begin{Bmatrix} \sigma_1 \\ \sigma_2 \\ \tau_{12} \end{Bmatrix} = [T_1] \begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix} \quad (4)$$

$$[T_1] = \begin{bmatrix} \cos^2\theta & \sin^2\theta & 2\sin\theta\cos\theta \\ \sin^2\theta & \cos^2\theta & -2\sin\theta\cos\theta \\ -\sin\theta\cos\theta & \sin\theta\cos\theta & \cos^2\theta - \sin^2\theta \end{bmatrix} \quad (5)$$

$$\begin{Bmatrix} \varepsilon_1 \\ \varepsilon_2 \\ \gamma_{12} \end{Bmatrix} = [T_2] \begin{Bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{Bmatrix} \quad (6)$$

$$[T_2] = \begin{bmatrix} \cos^2\theta & \sin^2\theta & \sin\theta\cos\theta \\ \sin^2\theta & \cos^2\theta & -\sin\theta\cos\theta \\ -2\sin\theta\cos\theta & 2\sin\theta\cos\theta & \cos^2\theta - \sin^2\theta \end{bmatrix} \quad (7)$$

Thus, the stress-strain relation for an orthotropic lamina oriented with respect to the x, y, z coordinate system is given by:

$$\begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix} = [T_1]^{-1} \begin{bmatrix} Q_{11} & Q_{12} & 0 \\ Q_{12} & Q_{22} & 0 \\ 0 & 0 & C_{66} \end{bmatrix} [T_2] \begin{Bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{Bmatrix} = [\bar{Q}] \begin{Bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{Bmatrix} \quad (8)$$

where $[\bar{Q}]$ represents the lamina-transformed reduced stiffness matrix.

For a relatively thin laminate, classical lamination plate theory can be used to predict the strains and stresses at any point across the thickness of a laminate undergoing deformation. The strain-displacement relation can be written in terms of midplane strains ($\{\epsilon^0\}$) and curvatures ($\{k\}$) as a function of an arbitrary distance, z , from the midplane of the laminate [41]:

$$\begin{Bmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{Bmatrix} = \begin{Bmatrix} \epsilon_x^0 \\ \epsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} + z \begin{Bmatrix} k_x \\ k_y \\ k_{xy} \end{Bmatrix} \quad (9)$$

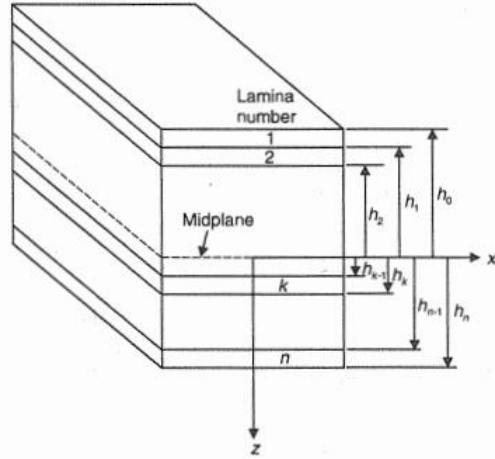


Figure 2.5: Schematic of a laminate consisting of n orthotropic laminae [41].

The variation of stresses at any point within a lamina, k , is provided by the following stress-strain relation (Figure 2.5):

$$\begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix}_k = \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix}_k \begin{Bmatrix} \epsilon_x^0 \\ \epsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} + z \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix}_k \begin{Bmatrix} k_x \\ k_y \\ k_{xy} \end{Bmatrix} \quad (10)$$

Next, the resultant forces $\{N\}$ and moments $\{M\}$ in the laminate are defined by the following integrations across the laminate thickness, h (Figure 2.5):

$$N_x = \int_{-h/2}^{h/2} \sigma_x dz \quad (11)$$

$$N_y = \int_{-h/2}^{h/2} \sigma_y dz \quad (12)$$

$$N_{xy} = \int_{-h/2}^{h/2} \tau_{xy} dz \quad (13)$$

$$M_x = \int_{-h/2}^{h/2} \sigma_x z dz \quad (14)$$

$$M_y = \int_{-h/2}^{h/2} \sigma_y z dz \quad (15)$$

$$M_{xy} = \int_{-h/2}^{h/2} \tau_{xy} z \, dz \quad (16)$$

For a laminate consisting of n orthotropic laminae, the force-moment system acting at the midplane of the laminate can be replaced with a summation of integrals where each represents the contribution of one layer:

$$\begin{aligned} \begin{Bmatrix} N_x \\ N_y \\ N_{xy} \end{Bmatrix} &= \sum_{k=1}^n \int_{h_{k-1}}^{h_k} \begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix}_k \, dz \\ &= \sum_{k=1}^n \left\{ \int_{h_{k-1}}^{h_k} \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix} \begin{Bmatrix} \epsilon_x^0 \\ \epsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} dz + \int_{h_{k-1}}^{h_k} \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix} \begin{Bmatrix} k_x \\ k_y \\ k_{xy} \end{Bmatrix} z dz \right\} \end{aligned} \quad (17)$$

$$\begin{aligned} \begin{Bmatrix} M_x \\ M_y \\ M_{xy} \end{Bmatrix} &= \int_{-h/2}^{h/2} \begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix} z \, dz = \sum_{k=1}^n \int_{h_{k-1}}^{h_k} \begin{Bmatrix} \sigma_x \\ \sigma_y \\ \tau_{xy} \end{Bmatrix}_k z \, dz \\ &= \sum_{k=1}^n \left\{ \int_{h_{k-1}}^{h_k} \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix} \begin{Bmatrix} \epsilon_x^0 \\ \epsilon_y^0 \\ \gamma_{xy}^0 \end{Bmatrix} z dz + \int_{h_{k-1}}^{h_k} \begin{bmatrix} \bar{Q}_{11} & \bar{Q}_{12} & \bar{Q}_{13} \\ \bar{Q}_{12} & \bar{Q}_{22} & \bar{Q}_{26} \\ \bar{Q}_{16} & \bar{Q}_{26} & \bar{Q}_{66} \end{bmatrix} \begin{Bmatrix} k_x \\ k_y \\ k_{xy} \end{Bmatrix} z^2 dz \right\} \end{aligned} \quad (18)$$

Noting that the midplane strains and plate curvatures remain constant for the whole laminate, they can be removed from the summation along with $[\bar{Q}]$, the reduced stiffness matrix. This allows for a simplified rewriting of the resultant forces and moments through the introduction of three new matrices, $[A]$, $[B]$, and $[D]$:

$$\begin{Bmatrix} N \\ M \end{Bmatrix} = \begin{bmatrix} A & B \\ B & D \end{bmatrix} \begin{Bmatrix} \epsilon^0 \\ k \end{Bmatrix} \quad (19)$$

$$A_{ij} = \sum_{k=1}^n (\bar{Q}_{ij})_k (h_k - h_{k-1}) \quad (20)$$

$$B_{ij} = \frac{1}{2} \sum_{k=1}^n (\bar{Q}_{ij})_k (h_k^2 - h_{k-1}^2) \quad (21)$$

$$D_{ij} = \frac{1}{3} \sum_{k=1}^n (\bar{Q}_{ij})_k (h_k^3 - h_{k-1}^3) \quad (22)$$

The [A], [B], and [D] matrices are respectively known as the laminate stiffness matrix, coupling stiffness matrix, and bending stiffness matrix, where h_k denotes the distance from the laminate mid-plane to the bottom of ply k (Figure 2.5).

2.3.2 Intralaminar and Interlaminar Damage Mechanisms in Laminated FRP Composites

Local intralaminar damage mechanisms, including breaking of fibres, microcracking of the matrix, and separation of fibre from the matrix (i.e., interfacial debonding), precede failure of a lamina [8]. Failure may be influenced by misalignment of fibres, variations in the strength of fibres, fibre-matrix interfacial conditions, and manufacturing-induced residual stresses [41]. In this section, the development of intralaminar damage in UD lamina under five in-plane loading conditions is reviewed, followed by a brief review of interlaminar damage (i.e., delamination).

Under longitudinal tensile loads, failure of a UD lamina begins with breakage of individual fibres. Fibre breakage increases with increasing load, leading to clustering of fibre breaks and adjacent matrix cracking in localized regions [58]. These regions eventually become too weak to support the increased load, causing complete separation of the composite (Figure 2.6a). The interfaces of broken fibres may debond due to stress concentrations at the fibre ends and contribute to composite separation (Figure 2.6b). Alternatively, cracks at different cross-sections may join up via debonding or by failure of the matrix (Figure 2.6c).

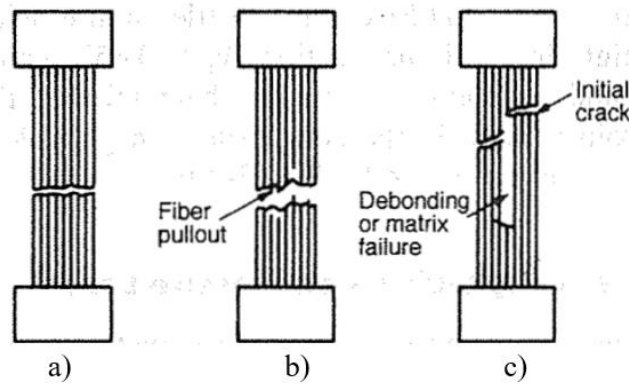


Figure 2.6: Possible failure modes of a UD composite under longitudinal tensile loading: (a) brittle failure, (b) brittle failure with fibre pullout, and (c) brittle failure with debonding and/or matrix failure [41].

When a UD lamina is subjected to longitudinal compressive loading, the continuous fibres act as long columns and in-phase fibre micro-buckling (i.e., kink-band formation) can occur (Figure 2.7a). For practical fibre volume fractions (greater than 40%), fibre micro-buckling is generally preceded by local matrix yielding, constituent debonding, and/or matrix microcracking. Transverse tensile strains caused by the Poisson effect can exceed the ultimate transverse strain capacity of the composite, resulting in cracks at the fibre-matrix interface. Shear failure is another potential mode of failure (Figure 2.7c).

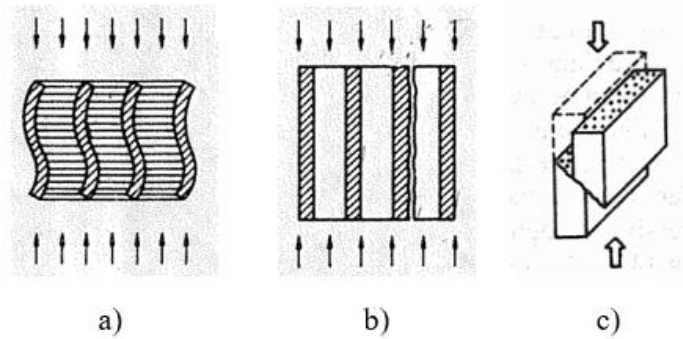


Figure 2.7: Possible failure modes of a UD composite under longitudinal compression loading: (a) in-phase fibre micro-buckling, (b) transverse tensile failure, and (c) shear failure. Images adapted from [41].

When a UD lamina is subjected to transverse tension loading, stress concentrations develop at the fibre-matrix interfaces. Thus, failure occurs because of the coalescence of local fibre-matrix interface cracks and microcracking within the matrix, which leads to a through-thickness ply crack (Figure 2.8). Under transverse compressive loads, a UD composite will undergo failure along a shear plane where localized matrix cracking and fibre-matrix debonding occur (Figure 2.9). Under in-plane shear loading, failure occurs by localized matrix shear failure and constituent debonding (Figure 2.10).

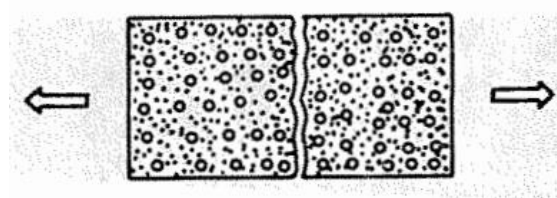


Figure 2.8: Failure of UD composite under transverse tensile loading [41].

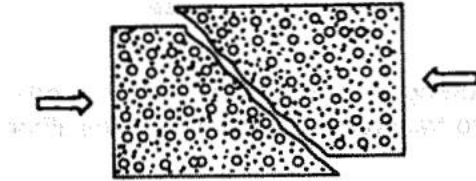


Figure 2.9: Shear failure of UD composite under transverse compressive loading [41].

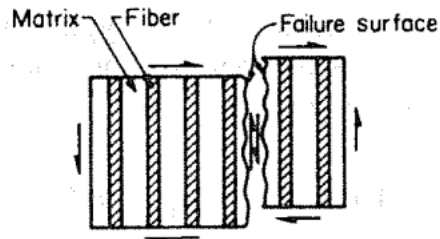


Figure 2.10: Failure of UD composite subject to an in-plane shear load [41].

Delamination cracks propagate between plies in a laminate and are induced by sufficiently high interlaminar stresses. Delamination cracks often initiate at the tips of intralaminar ply cracks as a result of stress concentrations at the ply crack tip (Figure 2.11).

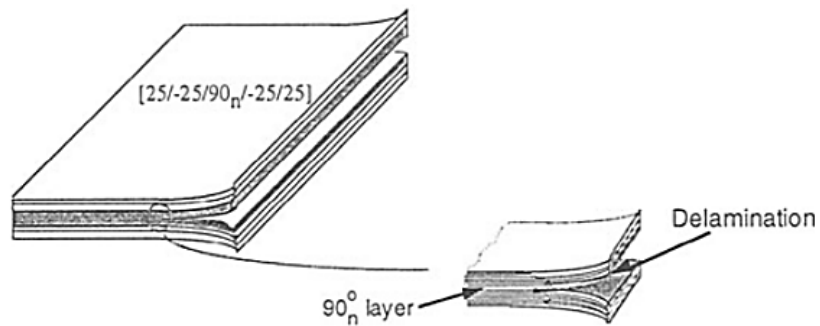


Figure 2.11: Schematic of delamination crack between two plies of a laminate [59].

2.3.3 Common Failure Theories

Common failure theories for fibre-reinforced plastic composites include the maximum stress, maximum strain, Tsai-Hill, Tsai-Wu, Hashin, and Chang-Chang criteria. All failure theories require the in-plane lamina stresses (Eq. (1)) and in-plane strengths corresponding to each mode of failure, including longitudinal tension, longitudinal compression, transverse tension, transverse compression, and shear. For the maximum stress failure theory, failure is predicted in a lamina if any of the in-plane stresses exceed the corresponding strengths [60]. Similarly, for the maximum strain failure theory failure is predicted in a lamina if any of the in-plane strains exceed the corresponding ultimate strains [60]. While the maximum stress and maximum strain theories inherently consider different modes of failure, they do not account for the interaction of failure modes and, thus, ignore the combined stress state of the lamina.

Interactive failure theories account for the coupling between lamina failure modes and are generally deemed to exhibit improved accuracy. The Tsai-Hill failure theory is an extension of the distortional energy yield criterion for isotropic materials and was extended for anisotropic materials [60]. A single polynomial that is a function of the in-plane stresses and strength describes the failure surface. The Tsai-Wu failure theory is based on the total strain energy applied to a lamina in plane stress and similarly uses a single polynomial function to describe the failure surface [60]. This theory is more general than the former theory because it distinguishes between the compressive and tensile strengths of a lamina. While the Tsai-Hill and Tsai-Wu failure theories are convenient, they do not account for different modes of failure. The Hashin and Chang-Chang failure theories consider different failure modes as well as the coupling between these modes. Both theories include criteria for fibre tensile failure, fibre compressive failure, matrix tensile failure, and matrix compressive failure. The theories are distinct from each other most significantly in their matrix damage criterion, although they have both been used to predict performance of composite materials under impact loading [61].

2.4 Energy Absorption Capacity of Composite Structures

FRP composites do not exhibit the same energy-absorbing mechanisms associated with ductile metallic alloys (i.e., plastic deformation) [14]. Instead, FRP composites primarily absorb energy through the formation of several distinct damage mechanisms (Section 2.3.2) as a result of their heterogeneous and layered structures [24]. The morphology of and interactions among these damage mechanisms are complex and may lead to distinct modes of failure, which are influenced by the fibre

architecture, lamina properties, laminate stacking sequence, part geometry, loading rate, and type of loading [7], [14], [32]. The following sections review the performance and associated failure modes for FRP composite parts under compressive axial and transverse bending loads.

2.4.1 Behaviour Under Compressive Axial Loading

Upon application of an axial compressive force, a FRP composite part may generally fail via buckling (leading to macroscopic fragmentation of the structure) or progressive crushing [24]. The latter is preferred and of interest as it corresponds to increased energy absorption under sustained loading. The general response of brittle FRP composites during progressive crushing is described herein, followed by a review of relevant studies.

2.4.1.1 Macroscopic Force-Displacement Response & Failure Modes

For a composite structure undergoing progressive crushing, the associated load-displacement curve can typically be divided into two distinct regions: pre-crushing and progressive-crushing regions [62], [63] (Figure 2.12). In the pre-crushing region, there is an initially elastic increase in the load corresponding to the contact of the impactor with one end of the channel. The applied load then reaches a peak corresponding to the beginning of failure, where local fracture initiates at the impacted end. Thereafter, the load drops to a lower plateau which corresponds to progressive crushing. In the progressive crushing region there are steady but random fluctuations in load, corresponding to the progressive failure of the specimen [63], [62]. This behaviour is due to the stick-slip nature of the crushing mechanism in which the stresses required to initiate crack growth are higher than those required for propagation [24], [14]. Minimal oscillations are ideal because they maximize the area under the force-displacement curve and thus the energy absorption.

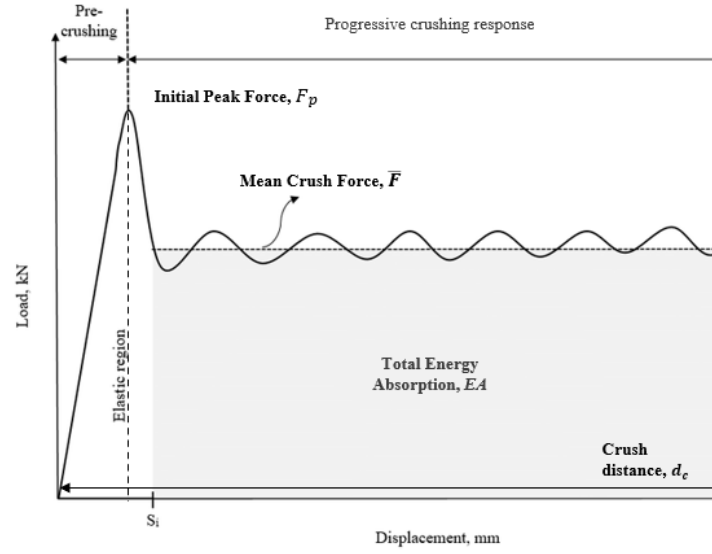


Figure 2.12: Typical force-displacement response of a brittle FRP failing via progressive crushing under axial compressive loads, adapted from [64].

There are several metrics commonly used to compare the energy-absorbing performance of composite structures, with the most notable summarized in Table 2.1 [7]. Specific or total energy absorption are the most widely reported parameters in the literature [14], [63]. However, crush force efficiency (CFE) is also an indicator of a good candidate for energy-absorbing applications as a lower CFE often leads to higher SEA [7]. There are no standard experimental tests to assess the crush resistance of FRPs which can make it difficult to compare studies investigating performance [63].

Table 2.1: Common performance metrics for evaluating the energy-absorbing capacity (EAC) of brittle FRP structures.

Performance Metric	Equation	Definition
Total energy absorption, EA	$EA = \int_0^{d_c} F(x)dx$	Integration of the crushing force with respect to the displacement of the impactor crushing the specimen. Area under the force-displacement curve up to the crush distance, d_c .
Specific energy absorption, SEA	$SEA = \frac{EA}{m_c}$	Energy absorption per crushed mass (m_c) of specimen

Mean crush force, $\bar{F}$	$\bar{F} = \frac{1}{d_c} \int_0^{d_c} F(x) dx$	Average crushing force; Total energy absorption divided by crush distance, d_c .
Initial peak force, F_p	-	Maximum force.
Crush force efficiency, CFE	$CFE = \frac{\bar{F}}{F_p}$	Ratio of mean crush force to peak force.

Both Hull [24] and Farley and Jones [65] characterized the progressive crushing behaviour of FRP tubes under axial compressive loads. Both reported two distinct failure modes – splaying/lamina bending and fragmentation/transverse shearing – with most specimens exhibiting a combination of both, denoted as brittle fracturing (Figure 2.13). Splaying/lamina bending is characterized by long interlaminar and intralaminar longitudinal cracks with little or no fracture of axial lamina bundles [14]. Crack growth is the principal energy absorption mechanism for this failure mode, with energy also being absorbed through the bending of lamina bundles and by frictional effects between the platen, fronds, debris wedge, and adjacent laminae [14], [62]. Fragmentation/transverse shearing is characterized by a wedge-shaped laminate cross-section, with one or more short interlaminar and longitudinal cracks forming partial lamina bundles. The wedge is created by the growth of interlaminar cracks which eventually cause the edges of the tube wall to fracture. The primary energy absorption mechanism is the fracture of lamina bundles, with interlaminar and longitudinal crack growth as secondary contributors [14]. Lastly, as mentioned, a catastrophic unstable failure is also possible (i.e. as a result of buckling) and leads to much lower energy absorption. Farley and Jones [65] suggest that unstable crack growth during lamina bending and constraining of interlaminar cracks in transverse shearing could lead to catastrophic failure.

Hull [24] proposed that the predominance of either splaying or fragmentation in a given tube is dictated by the micromechanics of fracture, which is influenced by the distribution of fibres in the composite. They demonstrated that tubes with a large proportion of hoop fibres will have a greater tendency to fail by fragmentation than those with a majority of axially oriented fibres. This is because the hoop fibres constrain against splaying and result in a relatively low through-wall shear strength. Carruthers [14] reviewed the energy absorption capacity and crashworthiness of composite material structures and suggested that the fragmentation mode appears to be characteristic of FRPs with high interlaminar and intralaminar shear strengths, while the splaying mode is more common with those of

correspondingly lower shear strengths. However, Carruthers also notes that the lamina bending failure tends to generate a larger crush area and so has a higher potential for absorbing energy by frictional effects at the platen specimen interface.

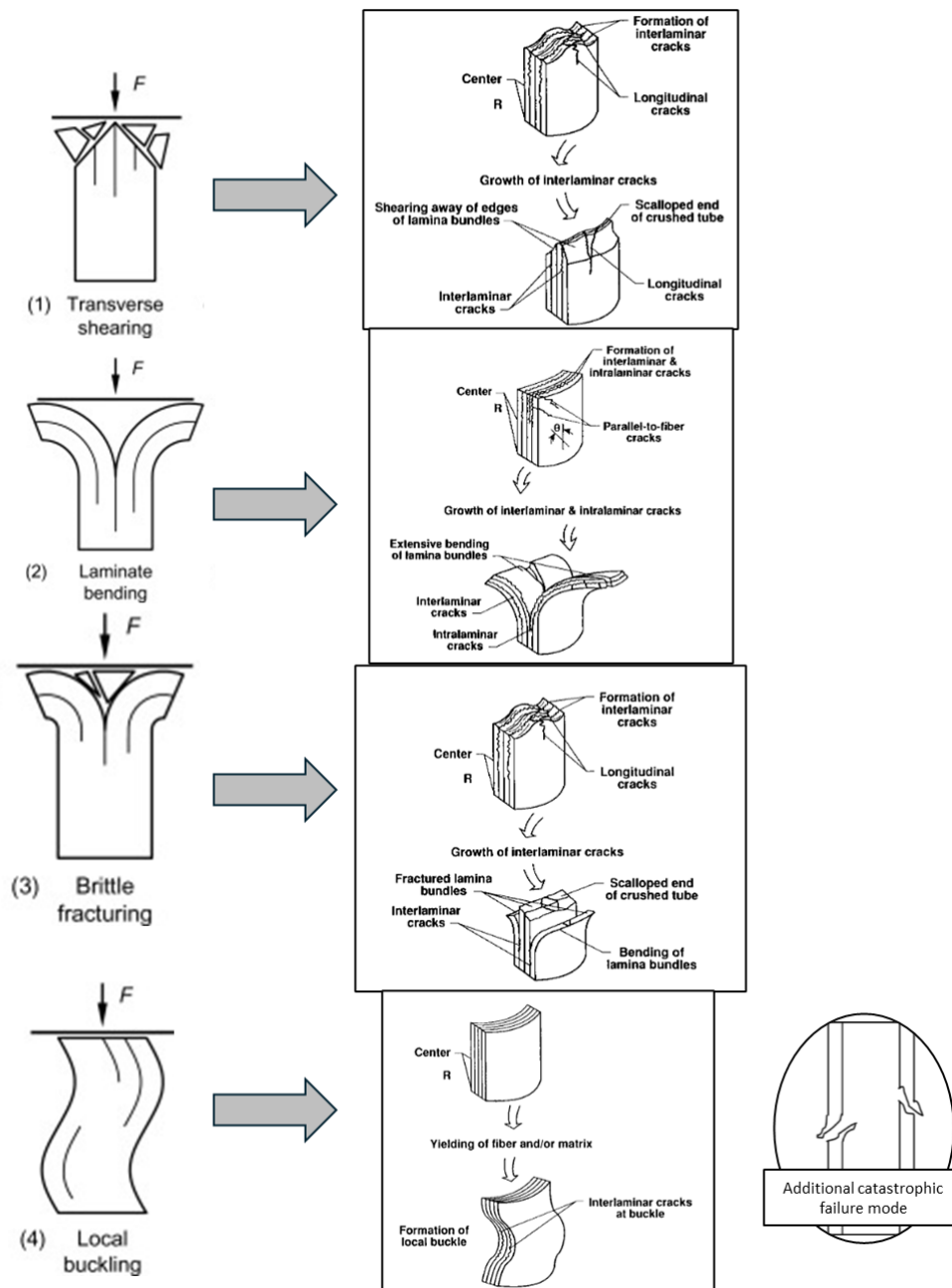


Figure 2.13: The failure modes possible for a brittle FRP composite parts under axial compressive loading, adapted from [40], [65].

2.4.1.2 Studies on Factors Affecting Performance Under Axial Loading

Several studies have aimed to assess the performance of FRP composite parts with various geometries, including circular [24], [66], [19], [67], [68], [69], [70], square [71], [39], [72], hexagonal and honeycomb [73], [74], and cone-shaped [28], [75], [70] tubes, as well as C-shaped channels [63], hat channels [62], [76], [77], [78], and corrugated channels [79], [39], [25]. In terms of thickness, Farley [80], Farley and Jones [65], and Mamalis et al. [81] studied the performance of circular fibreglass FRP composite tubes under quasi-static axial crush loads and reported similar trends of decreasing specific energy absorption with increases in the diameter-to-thickness ratio. The observation was attributed to a corresponding increase in the length and number of interlaminar cracks, which resulted in a reduced laminate failure strength. However, Farley and Jones [65] noted that very thick carbon fibre/epoxy tubes (diameter-to-thickness ratio greater than 1) did not readily crush progressively because of their inherent resistance to interlaminar crack growth. Instead, the tubes failed catastrophically when the hoop stress in the tube reached the strength of the material. Similarly, Carruthers et al. [14] noted that very thin-walled carbon fibre/epoxy tubes were prone to unstable failure via buckling.

Zhao et al. [82] experimentally and numerically (ABAQUS) investigated the crushing behaviour of woven CFRP tapered tubes with various cross-section profiles (Figure 2.14) under axial and oblique compressive loading. Experimental validation via dynamic drop testing at 5 m/s was only performed for the circular tapered tubes since these were estimated to possess the greatest EAC at both loading rates. It was concluded that increasing the tapered angle of the channels appeared to reduce unstable local buckling modes, which led to a proportional increase in SEA. In terms of oblique loading, the EAC of all channels was observed to decrease with increasing impact angle due to changes in the failure mode. With increasing impact angle, reduced fragmentation and delamination of the plies were predicted. Instead, increased unzipping of the tubes near the corners was observed (Figure 2.15). However, increasing the tapered angle was also found to reduce unstable crushing for all impact angles. It was concluded that intra-laminar interactions (energy absorbed by the intra-laminar fibre damage and in-plane shear plastic deformation) were the primary methods of energy dissipation regardless of impact loading angle. However, Zhao et al. also noted that profiles which resulted in increased delamination dissipated even more energy.

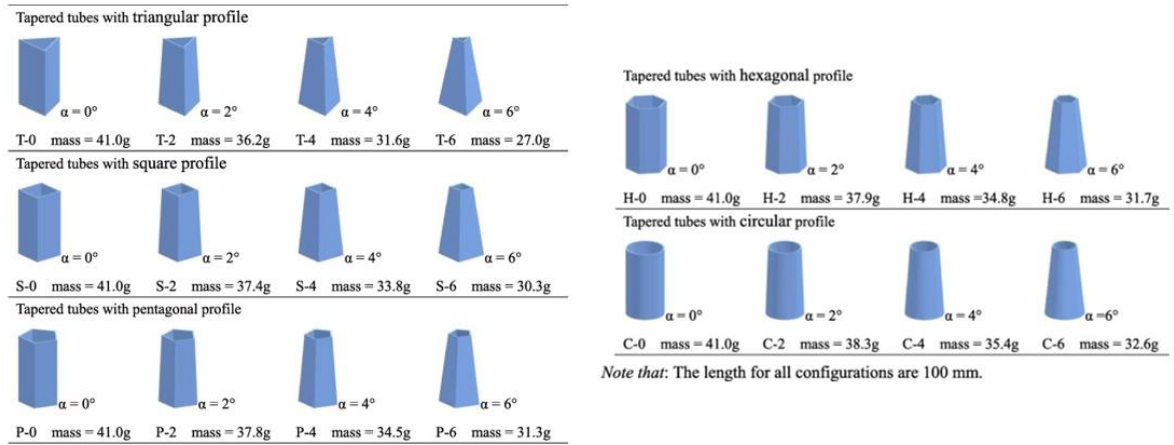


Figure 2.14: Geometries of CFRP tubes investigated by Zhao et al. [82] in ABAQUS to evaluate EAC.

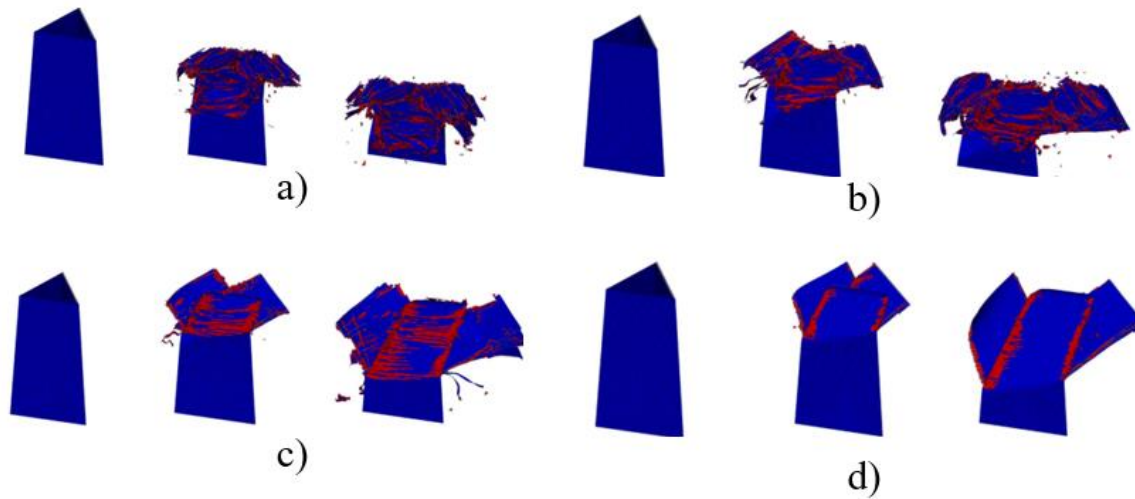


Figure 2.15: Predicted response of tapered CFRP tubes from simulation model developed by Zhao et al [82]. with increasing loading impact angles: (a) 0°, (b) 10°, (c) 20°, and (d) 30°. Adapted from [82].

Thornton and Edwards [83], and Mamalis et al. [84] both reported that square and rectangular tubes are generally less effective at absorbing energy when compared to circular tubes fabricated of glass, carbon or aramid prepreps under quasi-static axial and dynamic drop testing. It was suggested that this was typically because the corners of the tubes tended to act as stress concentrators, which led

to the formation of splitting cracks in the axial direction at the radii. The splits were noted to dominate the failure mechanism, resulting in a poor utilization of the material as a whole.

Harvey et al. [25] [76] studied the performance of UD-NCF carbon fibre/epoxy channels with hat and corrugated cross-sectional geometries (Figure 2.16) under dynamic axial compressive loading. Channels with two different stacking sequences, namely $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$, were manufactured using an HP-RTM process. It was found that per stacking sequence, the SEA of both geometries was similar (Figure 2.16). However, channels with the $[\pm 45/0_2]_s$ stacking sequence absorbed more energy than channels with the $[0/\pm 45/90]_s$ stacking sequence due to the increased number of plies in the axial direction. Fragmentation was the predominant failure mode observed for the $[0/\pm 45/90]_s$ channels, while some brittle fracture (combination of splaying and fragmentation) was observed for the $[\pm 45/0_2]_s$ channels.

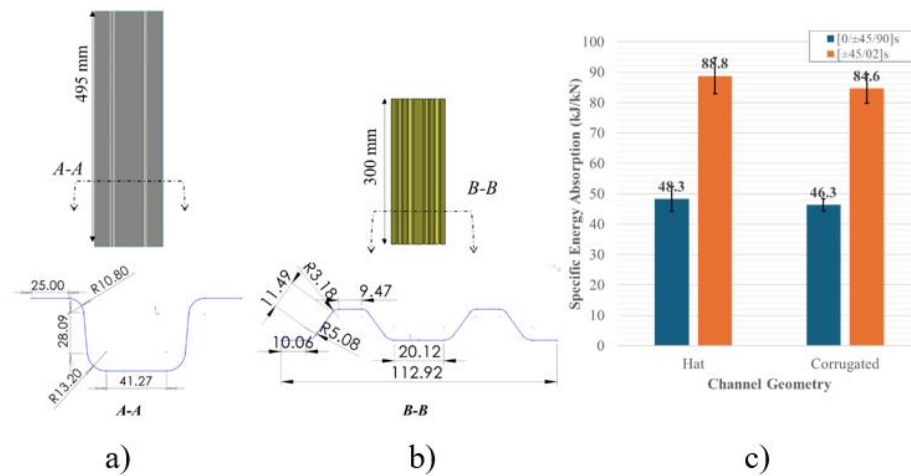


Figure 2.16: a) Hat channel geometry b) corrugated channel geometry c) Comparison of the specific energy absorption of UD-NCF carbon fibre/epoxy $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels subject to dynamic axial compressive loading [25], [76].

Similarly, Farley [80] performed quasi-static tests on $[0/\pm \theta]_3$ unidirectional carbon fibre/epoxy tubes for $15^\circ < \theta < 45^\circ$ and reported the greatest energy absorption for tubes with plies most closely oriented towards the direction of applied loading and notable decreases with increasing θ . However, Hull [24] demonstrated via quasi-static testing of $[0/90]$ carbon fibre/epoxy tubes with varied ratios and positions of hoop (90°) and axial (0°) fibres, that laminates consisting entirely of 0° plies (and axially loaded in the 0° direction) without any constraining outer (hoop) layers would yield

very low energy absorption via excessive longitudinal splitting and separation of plies. Furthermore, Farley [85], concluded from their quasi-static experiments on $[0/\pm\theta]_4$ and $[\pm\theta]_6$ carbon fibre/epoxy circular tubes where $\pm\theta$ was 15° , 45° , and 75° , that the relationship between energy absorption and ply orientation is highly dependent on the material system of the composites and, more specifically, the relative failure strains of the fibre and matrix. They noted that these will influence the stability of the tube and the type of crushing. It was suggested that when the failure strain of the fibres is less than the epoxy matrix, the matrix provides a degree of stabilization during the crushing process, and a subsequent decrease in energy absorption is due to the reduction in strength of the laminate. In this case, the energy absorption will decrease with an increasing angle. Conversely, when the fibres have a greater failure strain than the epoxy, the matrix fails first and thus provides little stabilization. In this case, stabilization is then provided by the hoop-oriented fibres, and the energy-absorbing capacity increases with increasing ply angles.

Ataabadi et al. [86] studied the effects of ply orientation and loading rate on the energy absorbing capacity of unidirectional carbon fibre/epoxy tubes and corrugated channels under quasi-static and low velocity impacts. Two types of stacking sequences were considered, angle ply ($[\pm 45/0/\pm 45/0/\pm 45/0/\pm 45]$) and cross-ply $[0/(90)_2/0]_s$. For the tubes, minor changes in energy absorption were measured with changes in loading rate and stacking sequence. However, significant differences in energy absorption were recorded for the open corrugated channels with changes in stacking sequence. The specific energy absorbed by the angle-ply channels was nearly 20 J/g greater than that of the cross-ply channels. Additionally, for each loading condition, the corrugated channels exhibited lower specific energy absorption than the corresponding tube tested. Lastly, the contribution of the outermost 0° layers towards the energy absorption of the tested parts was noted to be negligible as they delaminated readily, leading to extreme reduction of the load-carrying capacity. Thus, they increased the weight of the channels without increasing energy-absorbing capacity.

In a crash situation, high peak loads can be unfavourable for occupant protection [87]. FRP crush structures require a collapse trigger mechanism to initiate stable, progressive, high-energy crushing instead of catastrophic failure when subject to high peak loads [14]. Chamfers, slots, and tulip geometries have all been shown to work well as initiators [88]. Hull [24] demonstrated that in addition to reducing the peak load, chamfer triggers reduced the initial slope of the load-displacement curve because preliminary crushing would occur at the chamfer in addition to the elastic deformation of the tube. Additionally, the size of the load drop following initial failure was suggested to be

dependent on the angle of the chamfer and can be reduced to zero for some angles [14]. Thus, it is possible to optimize the geometry of a trigger for maximum energy absorption.

Siromani et al. [68] studied the effect of failure trigger mechanisms on the energy absorption capacity of circular carbon fibre/epoxy tubes subject to quasi-static axial compression. Chamfered-end, inward-folding or outward-splaying crush caps, and combined (chamfer-end and crush-cap) triggers were considered (Figure 2.17). It was found that the chamfer trigger was more effective than the crush caps on their own and that the combination of a chamfered tube and inward-folding crush cap yielded the lowest initial peak load and highest SEA.

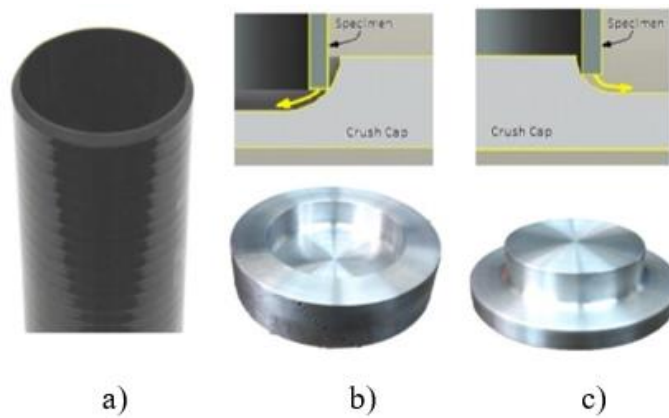


Figure 2.17: (a) Chamfered-end, (b) inward-folding and (c) outward-splaying crush caps, used by Siromani et al. [68] to investigate the effect of trigger geometry on the energy absorption of CFRP tubes subject to quasi-static compressive loading.

While the majority of studies are limited to quasi-static loading, increasing the loading rate can introduce material rate effects and inertial forces, which can influence the mode of deformation of an FRP composite component [51], [89]. Farley [90] found that $[0/\pm\theta]_2$ carbon fibre/epoxy tubes were insensitive to crushing speed, while a 35% rise in SEA was found in $[\pm\theta]_3$ tubes over a range of 0.1–12 m/s for θ equal to 15°, 45°, and 75°. It was proposed that if the mechanism controlling the crushing process was loading rate dependent, then so too would be the energy-absorbing capacity of the structure. More specifically, fracture of the lamina bundles was heavily influenced by the fibres in the 0° plies, the properties of which are not significantly influenced by the loading rate. In contrast, interlaminar crack growth is largely controlled by the matrix, which possesses strain-rate-dependent mechanical properties.

Hull [24] also investigated the performance of various channels subjected to axial compressive loading and suggested that friction in the crush zone may be another reason for changes in SEA with crushing speed. Frictional effects generate a considerable amount of heat, and the magnitude of energy absorbed by the mechanism will likely vary with the loading rate. In their investigation of the energy absorbed by frictional processes associated with axial loading of FRPs, Fairfull and Hull [91] found that friction could account for more than 50% of the total energy absorbed under progressive crushing, even when very smooth plates were used.

Hu et al. [19] studied the collapse characteristics and EAC of FRP composite tubes manufactured from five different types of epoxy-based prepregs. The first three tubes comprised satin woven carbon fabrics, the fourth tube consisted of unidirectional fabric, and the last tube comprised satin woven E-glass fabric. The tubes consisted of $[\pm 45]_n$ stacking sequences (where n is between 9 and 28) such that each channel was 3 mm thick with an inner diameter of 50 mm. A 45° chamfer was machined on one end, and the tubes were tested at quasi-static (10mm/min) and dynamic (drop-testing at 10.2 m/s) rates. It was found that dynamic drop testing resulted in lower energy-absorbing capacity in comparison to quasi-static loading. Additionally, it was observed that impact loading caused the crushing mode to transition from splaying to fragmentation. Lastly, carbon fibre/epoxy tubes were noted to absorb more energy than glass fibre/epoxy tubes.

In terms of varying the loading condition, Sun et al. [92] experimentally investigated the off-axis crushing behaviour of $[0/90]_5$ woven carbon fibre/epoxy tubes subject to quasi-static axial and oblique compressive loading (loading angles range of 0°–30°). It was noted that the composite tubes exhibited much more complex failure modes under oblique loading. Additionally, decreases in energy absorption were observed most significantly between 10° to 30° while little effect was noted between 0° to 10°. Boria et al. [28] investigated the effect of wall thickness, loading angle, and internal diameter on the energy absorption capacity of truncated woven carbon fibre/epoxy cones with quasi-isotropic stacking sequences, subject to quasi-static axial loading and dynamic drop testing. Increases in the peak force were observed with increases in thickness at both loading rates. For the quasi-static results specifically, an initial linear elastic region was noted, followed by oscillating loads that trend upwards with larger slopes corresponding to wider cone diameters. Lastly, a drop in stress was observed when the failure mode was characterized by the formation of fronds, followed by an almost constant mean crush force. Dynamically, it was noted that for some trials, initialization of crushing began from the bottom of the tubes instead of the top, which resulted in different force-displacement

responses. Additionally, since impact energy was fixed as opposed to the crushing distance, the area under each curve is equal and nearly constant between trials. Two notable failure modes were recorded: (a) splaying with axial splitting and frond formation, and (b) “total reversal of laminate” (Figure 2.18). The first mode was noted to correspond with improved energy absorption capacity. A 25% reduction in energy absorption was measured under the dynamic conditions noted. This was attributed to the filtering of dynamic signals and a difference in the coefficient of friction between impacting surfaces. Lastly, Boria et al. concluded that energy absorption can be maximized by increasing laminate thickness, increasing average diameter, and/or reducing wall inclination.

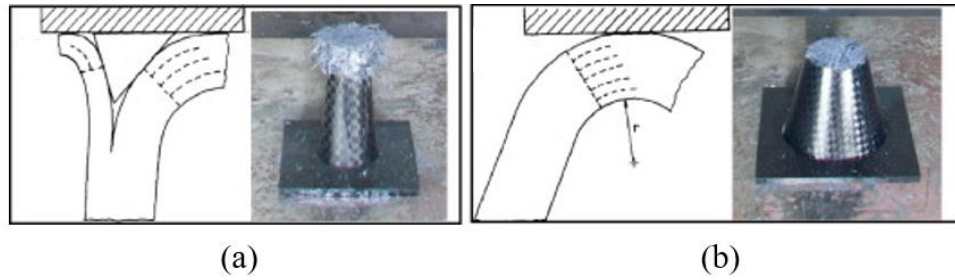


Figure 2.18: Failure modes distinguished by Boria et al. [28] from the quasi-static axial loading of truncated woven carbon fibre/epoxy cones: (a) splaying with axial splitting and external and internal frond formation, and (b) total reversal of the laminate internally to the specimen.

Wang et al. [26] studied the EAC of unidirectional carbon fibre/epoxy prepreg tubes and noted that EAC was significantly reduced (by 36%) under impact crushing conditions (drop hammer at 10.2 m/s) in comparison to quasi-static rates (10 mm/min). They thus suggested that dynamic results, instead of quasi-static results, should be used as a reference to design energy absorbers. However, where Liu et al. [27] (woven carbon fibre/epoxy double hat channels) and Boria et al. [28] demonstrated increasing SEA with increasing thickness in dynamic loading, Wang et al. noted decreasing SEA with increasing thickness.

Harvey et al. [25] [76] studied the effect of loading rate on the performance of UD-NCF carbon fibre/epoxy composite channels subject to both dynamic (7.5 m/s) and quasi-static (1mm/s) loading rates. Unlike most dynamic tests of composite channels, a large mass crash sled was used to crush the channels, which were fixed to a vertical reaction wall. This configuration is representative of the loading conditions of a crush tube undergoing a car crash. A displacement-controlled platen (hydraulic press) was used to load the channels quasi-statically. Channels with two stacking

sequences, namely $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$, were manufactured via HP-RTM with the same cross-sectional geometries shown in Figure 2.16. A difference in the failure mode was observed with a change in loading rate. Where fragmentation was the predominant failure mode observed during dynamic loading, extensive splaying was observed during quasi-static loading. Additionally, the energy-absorbing capacity at quasi-static loading was measured to be greater than at dynamic loading rates (Figure 2.19).

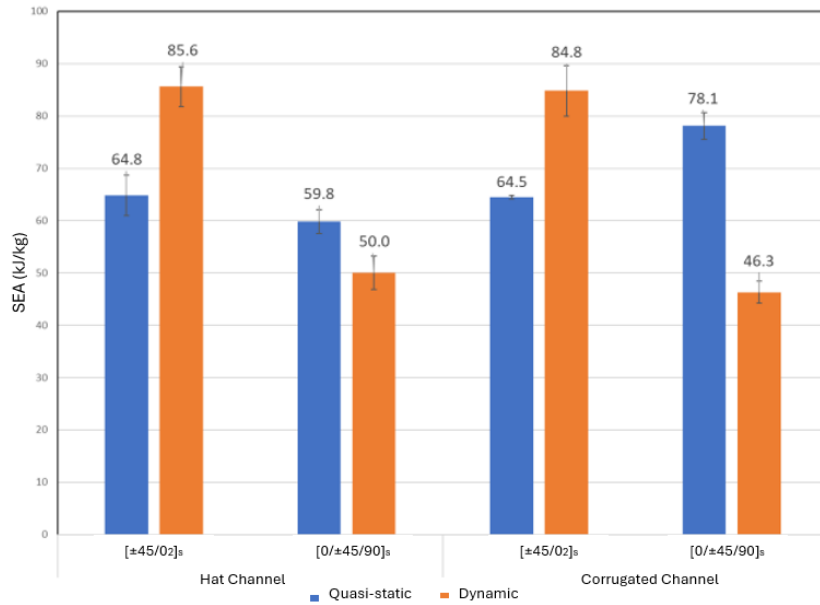


Figure 2.19: Specific energy absorption of UD-NCF carbon fibre/epoxy channels subject to dynamic and quasi-static axial compressive loading [25], [76].

2.4.2 Behaviour Under Transverse Bending Loading

Assessing the performance of composite structures under transverse loading is important to provide insight into the energy-absorbing capacity of an automotive FRP component subject to a side impact (i.e., CFRP B-pillars and side sill beams) [93]. In this section, the general response of brittle FRPs under compressive transverse loading is described followed by a review of relevant studies.

2.4.2.1 Macroscopic Force-Displacement Response & Failure Modes

The load-displacement response of an FRP channel under three-point bending exhibits two distinct regions: elastic bending and bending collapse [62], [78] (Figure 2.20). During the first region, the applied load increases until the first peak is reached and the average slope of the linear increase is

termed the flexural modulus of the channel [62]. After this first peak, local damage is observed at the location under the roller and the damage spreads quickly along the channel length, causing crack development at the side and bottom walls several sharp drops in the load-displacement curve. Amongst available literature, various performance metrics are used to compare the energy-absorbing capacity of the samples, including energy absorption until the peak force (beginning of bending collapse) [78], the entirety of the displacement (both regions of the force-displacement graph) [62], and the integration of the bending moment and angle of rotation [94].

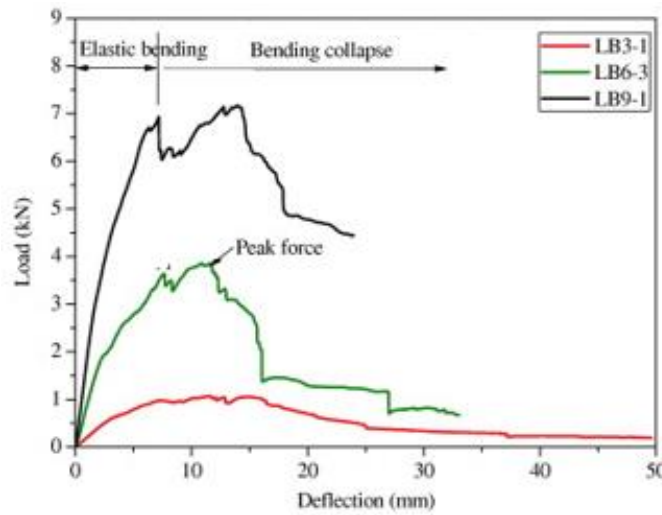


Figure 2.20: Load-displacement curves for the three-point bending of brittle FRP composite hat channels [62]. The response of composites with 3, 6, and 9 plies subject to quasi-static three-point bending are shown.

2.4.2.2 Studies on Factors Affecting Performance Under Transverse Loading

Liu et al. [62] performed quasi-static transverse bending tests on carbon fibre/epoxy double-hat channels with varying wall thicknesses. Tubes with three, six, or nine plies of plain weave carbon fibre/epoxy prepreg were manufactured via bladder within a hot press. Across all trials, progressive collapse was identified as the only failure mode accompanied by four distinct regions (Figure 2.21). The top wall was subject to compression, the bottom to tension, and the side wall and overlap areas to combined compression and tension. By varying the thickness of the composite tubes, it was concluded that increasing the thickness led to an increase in the peak load and specific energy absorption. Energy absorption appeared to have been measured as the integration of the entirety of the

load-displacement curve, but this was not explicitly described in the paper. Furthermore, it was noted that the cracks developed differently between the tested specimens (i.e., on one specimen, cracks do not appear on the bottom wall and in another, it is deeply collapsed).

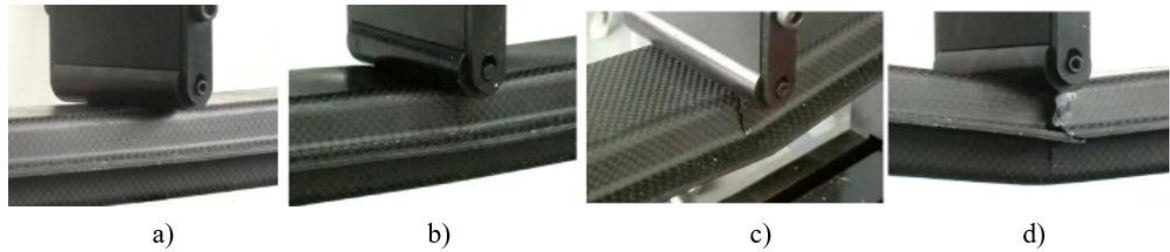


Figure 2.21: Progressive bending collapse process of plain weave carbon fibre/epoxy double-hat channels quasi-statically tested by Liu et al. [62]: (a) elastic bending, (b) crack initiation, (c) side wall rupture and (d) bottom wall rupture. Adapted from [62].

Chen et al. [78] characterized the crush responses of carbon fibre/epoxy and glass fibre/epoxy hat-shaped channels and flat plates (Figure 2.22) subject to quasi-static three-point bending and transverse compression. The influence of the number of plies, ply orientation, and inter-ply structure (i.e. with or without a sandwich core) on the performance of the channels was considered. $[0/90]_n$ layups where n represented 6, 8 or 10 plies, and $[\pm 45^\circ]_{5s}$ laminates were manufactured. Performance was evaluated by comparing the load-displacement curves, where energy absorption was calculated as the integration of the load-displacement curve up to the peak force. Chen et al. concluded that the failure modes varied with ply angle for three-point bending but remained constant under transverse compression. Failure was described as being dominated by fibre breakage and partial delamination. Increasing thickness was found to improve energy absorption, and the addition of $\pm 45^\circ$ layers improved three-point bend performance. The use of sandwich structures did not improve energy absorption.

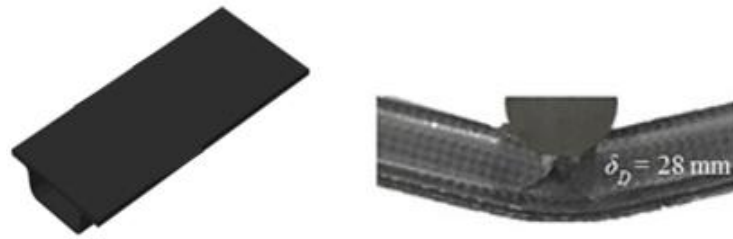


Figure 2.22: Example of the CFRP hat channel and flat plate studied by Chen et al. [78] under quasi-static three-point bending and transverse compression, adapted from [78].

Zhou et al. [95] studied the energy absorption of $[0/90/0_3]_s$ and $[0/\pm 60]_{2s}$ unidirectional carbon fibre/epoxy hat channels subject to dynamic three-point bending at impact velocities between 3.7 m/s and 6.5 m/s. Flat plates with the corresponding stacking sequence were adhered to the bottom of each hat channel to improve the success of testing. The effect of impact velocity was found to be minor, and all channels exhibited similar failure modes. However, an increase in damage with increasing impact velocity was observed.

Harvey [25] studied the performance of $[\pm 45/0_2]_s$ and $[0/\pm 45/90]_s$ UD-NCF hat and corrugated channels manufactured via HP-RTM, subject to three-point bending under dynamic and quasi-static loading rates. Similar failure modes were reported for all tested channels, namely, fracture of the channel at the location where it was impacted by the cylinder (Figure 2.23). The $[\pm 45/0_2]_s$ channels exhibited more energy absorption than the $[0/\pm 45/90]_s$ channels, while the peak force tended to be higher under dynamic loading. The influence of loading rate on specific energy absorption did not follow a clear trend for the channels tested.

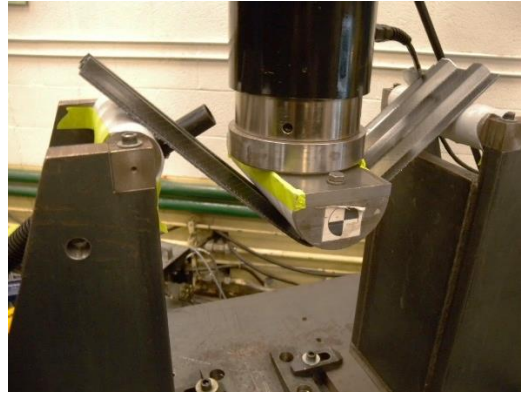


Figure 2.23: Failure of $[\pm 45/0_2]_s$ UD-NCF corrugated channel subjected to quasi-static three-point bending [25].

2.5 Predicting the Performance of FRP Composites via Simulations

Developing simulation models to predict the failure of FRP composite materials under crash conditions is challenging because it requires modelling failure initiation and damage propagation (i.e., post-peak response) [31]. Several damage mechanisms may manifest in FRP composites (Section 2.3.2) and it is difficult to capture all mechanisms in a single analysis [32]. Commercially available codes used for crash simulations of FRP composite structures include LS-DYNA [96], Abaqus Explicit, RADIOSS, and PAM-CRASH [31], [32]. These codes contain various available material models to represent CFRP composites, via failure criteria, material property degradation schemes, and model-specific input parameters [32].

The constitutive models can be divided into continuum damage models (CDMs) and progressive failure models (PFMs). CDMs simulate damage as a gradual degradation of material properties that continues to reduce stiffness as damage evolves. This is achieved through the introduction of damage variables within the constitutive equations of the material. CDMs provide more accurate and detailed representations of the damage evolution process but are also computationally expensive. This renders them unsuitable for large-scale simulations. In contrast, PFMs simulate failure as a series of distinct events (i.e. delamination, matrix cracking, fibre breakage) which results in the sequential deletion of elements as they meet failure criteria [97]. The failure criteria used in PFMs are typically strength-based, and the stress-strain responses do not require that specific unloading/softening curves be assigned [98]. After the strength of the ply exceeds the defined properties, the properties of the ply suddenly drop to zero. PFMs are best suited to large-

scale simulations where simplicity and computational efficiency are prioritized over detailed material behaviour. However, they often oversimplify crushing phenomena and require extensive calibration [97]. Both PFMs and CDMs have been developed and implemented within the commercial software package, LS-DYNA. Examples of PFMs in LS-DYNA include MAT_022 and MAT_054, while examples of CDMs include MAT_058 and MAT_162 [96].

To accurately capture the behaviour of composite materials, it is also necessary to identify the appropriate material length scales that must be considered [99] (Figure 2.24). Given the current study's focus on energy-absorbing FRP composite components, modelling at the macro-scale is most appropriate because it includes consideration of the assembly of structural elements with boundary conditions and loads. As a result, models which consider the individual fibres, matrix and their interface (micro-scale) were not considered herein.

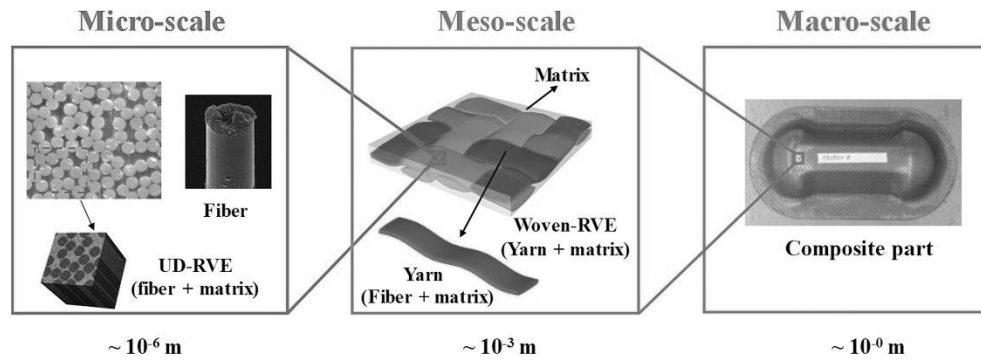


Figure 2.24: Example of three different scales for modelling a woven fabric composite [100].

At the macro-scale, available material models can capture the behaviour of laminated FRP composite structures using either a single layer of shell elements [37], [32], [101], multiple layers of shell elements [99], [102] or 3D solid elements [102] (Figure 2.25). However, codes such as LS-DYNA, which was used in the present study, utilize explicit time integration schemes for dynamic problems and require small time increments [31]. This can easily become computationally expensive; thus, shell elements are commonly used to perform component-level and full vehicle simulations [31]. Single-layer models are a reasonable choice for simulating the crush response of an FRP component. However, it is worth noting that single-layer models cannot capture delamination [99]. This can be addressed by connecting layers of shell or solid elements, each representing a ply via tiebreak contacts or cohesive zone modelling [99]. Lastly, solid elements can be useful for modelling thick composites, where the through-thickness stress tensor components are no longer negligible [102]. A

summary of the existing material models that capture failure of FRP composites and are compatible with single-layer shell models in LS-DYNA is provided hereafter, followed by a review of relevant studies.

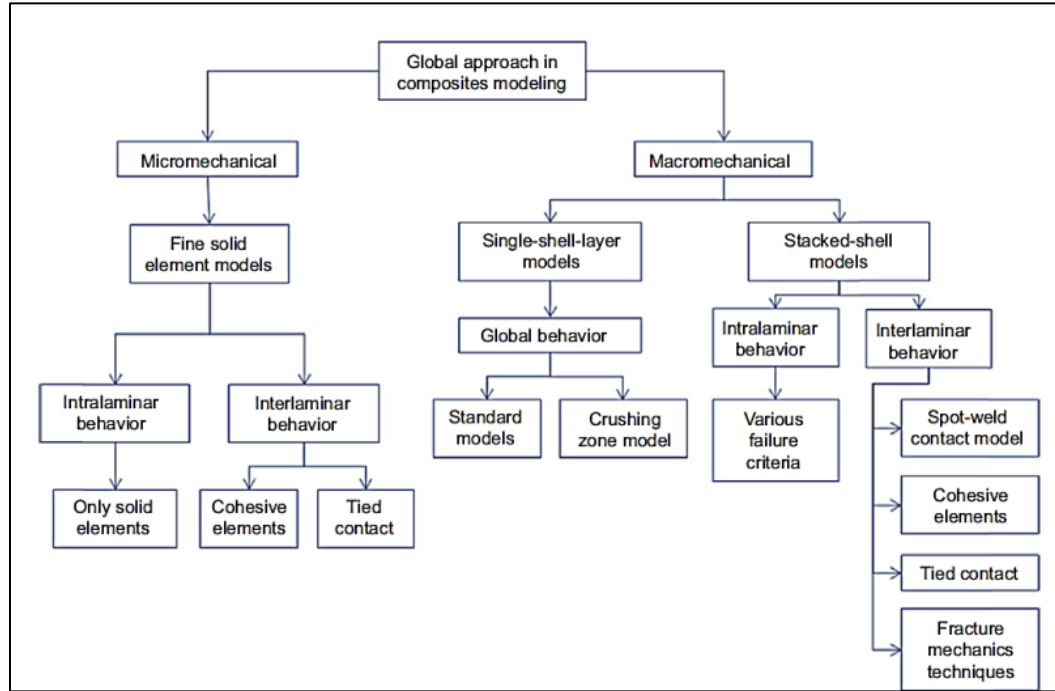


Figure 2.25: Classification of numerical modelling approaches for modelling the behaviour of composite materials [103].

2.5.1 Overview of Available Composite Material Models in LS-DYNA

MAT_022 is a simple linear elastic orthotropic progressive failure material model for lamina that employs the Chang-Chang's failure criteria [104]. The material model is dependent on four strength parameters, namely longitudinal tensile strength, transverse tensile strength, transverse compressive strength, and in-plane shear strength, and a non-linear shear stress parameter to calibrate fibre tension, matrix tension, and matrix compression failure criteria [105]. Elements depict sudden failure after exceeding the strength limits of the ply based on the ply discount method. The model does not consider fibre compressive failure and would not be well applied to compressive loading models.

MAT_054 and MAT_055 are enhanced versions of the material model, MAT_022, that consider fibre compressive failure and are widely used to simulate the impact of FRP composite structures. MAT_054 and MAT_055 are progressive failure models, requiring the iterative calibration of several parameters to achieve agreeable results with experimental data. The selection of failure criteria distinguishes MAT_054 and MAT_055, where Chang-Chang is used for the former and the Tsai-Wu failure criteria for the latter [104]. Linear elastic deformation is assumed until failure is predicted. At failure, the corresponding elastic properties go to zero. Options to reduce the maximum stress by a predefined factor can be included within the post-peak response (SLIM parameters), followed by an assumed perfectly plastic response (i.e., constant stress) (Figure 2.26). Additional details on MAT_054, the material model calibrated for this study, are provided in Section 2.5.2

MAT_058 is an elastic continuum damage mechanics-based material model for lamina that captures pre-peak softening [104] and subsequent stiffness degradation as a result of damage [106]. The four failure modes considered within the Hashin failure criteria [107] act as threshold variables in the damage model for failure initiation: fibre tension (rupture), fibre compression (kinking), matrix tension (cracking) and matrix compression (crushing) [36]. After these are initiated, there is a degradation in elastic properties. The most significant difference relative to MAT_054 is the smooth (non-linear) increase of damage as opposed to a sudden change in material behaviour; where a smooth increase appears more physically correct [106] (Figure 2.26). Options for setting stress limits (SLIMx), similar to MAT_054, are provided. These limit the stress in the post-peak softening to a given value. The current version of MAT_58 provides options to capture the strain rate-dependent moduli and strengths.

MAT_213 is a lamina-based orthotropic elastoplastic damage material model for FRP composites [104]. The material model is divided into deformation, damage, and failure sub-models [108]. The nonlinear pre-peak softening response caused by local yielding and damage accumulation is captured by the deformation sub-model using an elastoplastic deformation model. Nonlinear unloading/reloading due to stiffness reductions is captured by the damage sub-model. The erosion of elements from the simulation model is accomplished by the failure sub-model and post-peak response is not considered. Temperature and rate-dependent stress-strain data as well as the characterization of matrix damage and failure are required. The yield function is based on a modified form of the Tsai-Wu failure criterion and requires the calibration of more than 100 parameters [31].

MAT_219 uses a continuum damage mechanics approach for fibre-reinforced laminates where nonlinear behaviour arises out of damage evolution in defined sub-laminates [109], [110]. Damage initiation is predicted using mode-dependent strain-based criteria, and damage growth is captured by the UBC Composite Damage Model (CODAM2) [91], [92] [35], [111]. The model does not consider strain rate dependency of the material response and requires testing of the specific laminate under consideration for calibration of the laminate's post-peak response.

MAT_261 [104] is an orthotropic continuum damage model for lamina. Failure initiation is predicted using physically based failure criteria developed by Pinho et al. [33], while the post-peak response for each deformation mode is governed by experimentally measured intralaminar fracture toughness. A smeared formulation was used to avoid mesh dependency during damage propagation [33]. Strain rate effects and pre-peak softening are not considered in this model. MAT_262 is a similar CDM-based material model with physical-based failure criteria and simplified non-linear in-plane shear behaviour [104]. MAT_262 differs from MAT_261 in that bi-linear post-peak softening for the longitudinal direction and linear softening for the transverse and shear directions are assumed [112] (Figure 2.26). Additionally, damage activation functions based on the LaRC04 failure criteria are used to predict the different failure mechanisms occurring at the ply level.

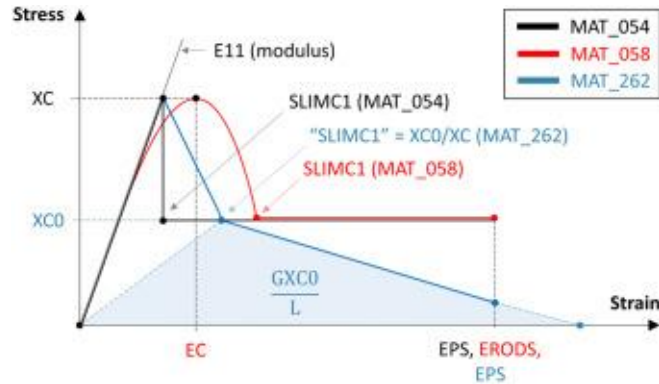


Figure 2.26: Comparison of stress-strain response with material models, MAT_064, MAT_058, and MAT_262, in LS-DYNA [38].

2.5.2 Additional Details on MAT_054

MAT54 has been widely used to simulate damage progression in dynamic failure simulations because it requires a reduced number of experimental input parameters compared to damage mechanics-based material models [39]. Many researchers have utilized MAT_054 to simulate the response of composite structures on a component level as well as within large-scale crash simulations [101], [67].

For a unidirectional lamina with the fibre as direction-1, the material elastic stress-strain response is governed by [104]:

$$\varepsilon_1 = \frac{1}{E_1}(\sigma_1 - \nu_{12}\sigma_2) \quad (23)$$

$$\varepsilon_2 = \frac{1}{E_2}(\sigma_2 - \nu_{12}\sigma_1) \quad (24)$$

$$2\varepsilon_{12} = \frac{1}{G_{12}}(\tau_{12} + \alpha\tau_{12}^3) \quad (25)$$

where ε_1 , ε_2 , and ε_{12} represent the strains in the fibre, matrix, and shear directions, E_1 , E_2 , and G_{12} represent the longitudinal, transverse, and shear moduli, σ_1 , σ_2 , and τ_{12} represent normal stresses along the fibre and transverse directions and in-plane shear stresses, ν_{12} is the in-plane Poisson's ratio, and α is a nonlinear shear stress parameter [104].

The Chang-Chang failure criteria consider tensile fibre, tensile matrix, compressive fibre and compressive matrix modes of failure. Once a failure mode is predicted, the corresponding lamina engineering constants are reduced to zero. For the tensile fibre mode where $\sigma_1 > 0$, the criterion is given by [104]:

$$e_f^2 = \left(\frac{\sigma_1}{X_t}\right)^2 + \beta \left(\frac{\sigma_{12}}{S_c}\right)^2 - 1 \begin{cases} \geq 0 & \text{failed} \\ < 0 & \text{elastic} \end{cases} \quad (26)$$

where X_t is the fibre tensile strength, S_c is the shear strength, and β is the weight factor for the shear term. For $\beta = 1$, this criterion becomes the Hashin criterion [112] for tensile fibre mode. For $\beta = 0$, this criterion becomes the maximum stress criterion [104]. If failure is predicted, the engineering constants are set as $E_1 = E_2 = G_{12} = \nu_{21} = \nu_{12} = 0$.

For the compressive fibre mode where $\sigma_1 < 0$, the criterion is given by:

$$e_c^2 = \left(\frac{\sigma_1}{X_c}\right)^2 - 1 \begin{cases} \geq 0 & \text{failed} \\ < 0 & \text{elastic} \end{cases} \quad (27)$$

where, X_c is the fibre compressive strength. If failure is predicted, $E_1 = \nu_{21} = \nu_{12} = 0$.

For the tensile matrix mode where $\sigma_2 > 0$, the failure criterion is given by:

$$e_m^2 = \left(\frac{\sigma_2}{Y_t}\right)^2 + \beta \left(\frac{\sigma_{12}}{S_c}\right)^2 - 1 \begin{cases} \geq 0 & \text{failed} \\ < 0 & \text{elastic} \end{cases} \quad (28)$$

where, Y_t is the matrix tensile strength. If failure is predicted, the engineering constants are set as $E_2 = \nu_{21} = 0 \rightarrow G_{12} = 0$.

For the compressive matrix mode where $\sigma_2 < 0$, , the failure criterion is given by:

$$e_d^2 = \left(\frac{\sigma_2}{2S_c}\right)^2 + \left[\left(\frac{Y_c}{2S_c}\right)^2 - 1\right] \frac{\sigma_2}{Y_c} + \left(\frac{\sigma_{12}}{S_c}\right)^2 - 1 \begin{cases} \geq 0 & \text{failed} \\ < 0 & \text{elastic} \end{cases} \quad (29)$$

where, Y_c is the matrix compressive strength. If failure is predicted, the engineering constants are set as $E_2 = \nu_{21} = \nu_{12} = 0 \rightarrow G_{12} = 0$.

The material inputs of MAT_054 can be organized into the following categories: engineering constants, material axes definitions, shear weighting factors, material strengths, failure criterion selections, damage factors, deletion factors, and stress limits (Figure 2.27) [32], [104]. Of the categories, all but the first two must often be calibrated iteratively using component-level simulations and are thus denoted “non-physical” parameters. Most notable are the damage (e.g., SOFT) and deletion factors (e.g., DFAILx), which reduce the strength of the elements with the accumulation of damage, and control the strain at which integration points fail, respectively. Additional information is provided below as required.

Constitutive properties	Shear weighing factor	Materials Strengths	Failure criterion selection	Material axes definition	Damage factors	Deletion factors	Stress Limits
MID , LS-DYNA Material ID Number	RO , Density	EA , Youngs Modulus in longitudinal direction, E_1	EB , Youngs Modulus in longitudinal direction, E_2	EC , Youngs Modulus in longitudinal direction, E_3 (not used)	PRBA , Minor Poisson's Ratio, ν_{21}	PRCA , Minor Poisson's Ratio, ν_{23}	PRCB , Minor Poisson's Ratio, ν_{31}
GAB , Shear Modulus G_{12}	GBC , Shear Modulus G_{23}	GCA , Shear Modulus G_{32}	KF , Bulk Modulus (Not used)	AOPT , Material axes option	2WAY , Flag to turn on 2-way fiber action.		
XP, YP, ZP , Coordinates for AOPT-1			A1, A2, A3 , AOPT=2 a vector components			MANGLE , AOPT=0 Material angle	
V1, V2, V3 , AOPT=3 v vector components			D1, D2, D3 , AOPT=2 d vector components			DFAILM , Matrix maximum strain in tensile and compressive modes	DFAILS , Maximum tensorial shear strain
TFAIL , Elements with time steps less than TFAIL are deleted.	ALPH , Nonlinear shear stress parameter (see failure criteria)	SOFT , Reduction of material strength (softening) in elements that shared nodes with deleted elements.	FBRT , Reduction of fiber tensile strength, after matrix compressive failure; $XT = XT * FBRT$	YCFAC , Softening for fibre compressive strength after matrix compressive failure. $Xc = YCFAC * Yc$	DFAILT , Maximum failure strain in fibre tension.	DFAILC , Maximum strain in fibre compressive strength	EFS (Effective failure strain.) Definition of failure strain in any direction/mode for matrix or fibre.
XC , Compressive strength in fibre direction	XT , Tensile strength in fibre direction	YC , Transverse compressive strength (matrix)	YT , Transverse tensile strength (matrix)	SC , Shear strength	CRIT , Failure criterion	BETA , Weighting factor for shear term in tensile fibre mode	
PFL , Percentage of layers to fail to initiate crash front	EPSF , Transverse Shear strain damage initiation	EPSR , Transverse shear strain for fibre rupture	TSMD , Transverse shear maximum damage	SOFT2 , Orthogonal softening reduction factor			
SLIMT1 , Factor by which maximum stress in element is reduced at failure, in fiber tension.	SLIMC1 , Factor by which maximum stress in element is reduced at failure in fiber compression.	SLIMT2 , Factor by which maximum stress in element is reduced at failure, in matrix tension.	SLIMC2 , Factor by which maximum stress in element is reduced at failure, in matrix compression.	SLIMS , Factor by which maximum stress in element is reduced at failure, in maximum shear.	NCYRED , Number of cycles for stress reduction from maximum to minimum.	SOFTG , Softening factor for transverse shear moduli in crash front elements.	
LCXC , Load curve ID for XC vs Strain rate	LCXT , Load curve ID for XT vs Strain rate	LCYC , Load curve ID for YC vs Strain rate	LCYT , Load curve ID for YT vs Strain rate	LCSC , Load curve ID for SC vs Strain rate	DT , Strain rate averaging option.		

Figure 2.27: Input parameters for the material model, MAT_054, available in LS-DYNA as presented in LS-Prepost. Note, the directions A, B, C, in the material model correspond to the directions, 1, 2, 3, of a lamina.

After the reduction in lamina engineering constants as a result of lamina failure per the aforementioned failure criteria, the material behaviour becomes either perfectly plastic or there is a drop in stress to a residual value (controlled by the relevant SLIMx parameter), followed by a perfectly plastic response [37]. For MAT_054, final failure of a ply (integration point) can occur in one of four ways:

- 1) If DFAILT = 0, failure occurs if the Chang-Chang failure criterion is satisfied in the tensile fibre mode (Eq. (27))
- 2) If DFAILT > 0, failure occurs if the tensile fibre strain is greater than DFAILT or less than DFAILC
- 3) If EFS > 0, failure occurs if the effective strain is greater than EFS
- 4) If TFAIL > 0, failure occurs if the time step of the element is smaller than TFAIL.

When the failure of all plies (integration points) in an element has been predicted, the element is deleted in the simulation model. Elements which share nodes with the deleted element become “crash front” elements and their strength is reduced based on the SOFT parameter (a value between 0 and 1). Information about each layer/integration point can be plotted using additional integration point variables (NEIPS in *DATABASE_BINARY for shells) (Appendix A).

2.5.3 Studies on Simulation Model Development with Material Model MAT_54

Many studies have attempted to calibrate MAT_054 from the available experimental data of a specific FRP composite material, and then use the tuned material model within simulation models to predict the energy-absorption capacity of FRP composite components subject to impact loads [75], [113], [114], [115], [116].

Mamalis et al. [72] developed simulation models to predict the axial crush response of square woven carbon fibre/epoxy tubes subject to quasi-static (7 mm/min) and dynamic (5400 mm/s) loading rates. Material models, MAT_054, MAT_055, and MAT_058, were considered and tuned until good agreement was achieved. Laminate splaying, local tube-wall buckling, and mid-length collapse were captured in the simulations and were observed during quasi-static testing (Figure 2.28). To capture splaying, Mamalis et al. modelled three layers of rectangular shell elements and used single-layer models for the other two modes. This approach reflected the observed collapse mode, which splayed into two bundles of plies with the simultaneous formation of a debris wedge. Numerically, the thickness of the outer layers of shell elements was set to slightly less than half of the tube wall

thickness, and the middle layer was of negligible thickness. Material model, MAT_055, was found to best capture the experimentally observed brittle crushing response of the composite tubes.

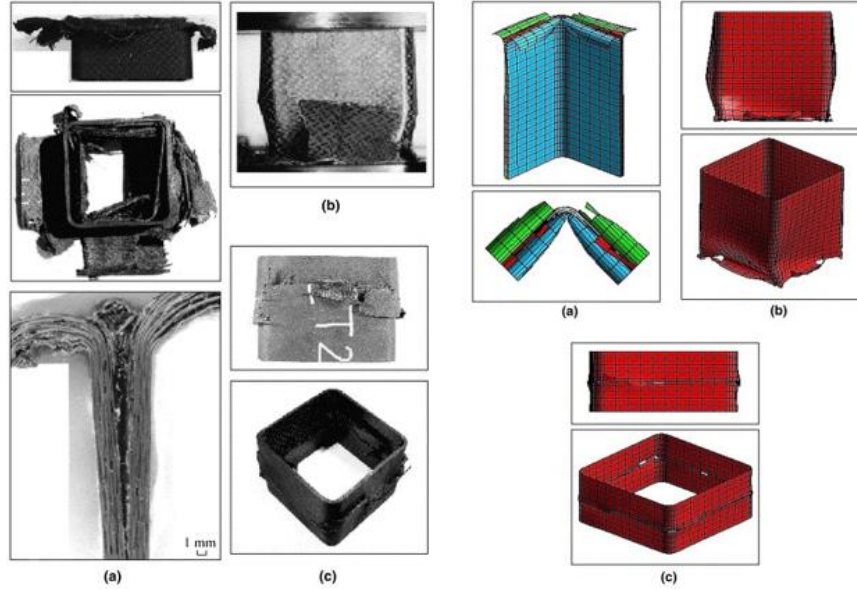


Figure 2.28: Carbon fibre/epoxy tubes subject to quasi-static axial loading: experimental tests and finite element representation developed in LS-DYNA, by Mamalis et al. [72].

Cherniaev et al. [38] investigated the use of MAT_054, MAT_058, and MAT_262 to predict the axial crush response of $[0_2/45_2/90_2]_s$ square UD carbon fibre/epoxy tubes axially compressed at a load rate of 5.5 m/s. All channels were generated as single-shell parts to maximize efficiency, and parameter tuning was performed for each material model using available axial crush experiment results. It was concluded that MAT_054 provided the best ratio of prediction accuracy to parameter tuning of all three material models. Additionally, the stress limit factor in longitudinal compression (SLIMC1) and the crash front softening factor (SOFT) were noted to be the most influential non-physical parameters. A single shell layer approach was noted to have led to a significant underestimation of energy absorption when fibre splaying was the dominant model of crushing. As a result, Cherniaev et al. [38] recommended implementing stacked shell models. Lastly, they recommended out-of-plane randomization of nodal coordinates to improve the physical representation of local force peaks or other manufacturing-induced defects.

Huang and Wang [113] modelled the response of $[\pm 45/90/0_2/90/0]_s$ carbon fibre/BMI circular tubes axially loaded at quasi-static rates (2mm/min) in LS-DYNA. Two layers of Belytschko-

Tsay shell elements were created to represent the tubes, and fully integrated elements were chosen to represent the 45° chamfer machined for experimental testing. The two layers were selected to better capture delamination, as observed experimentally, while also minimizing increases to computation time. Similarly, mass scaling was used to reduce time, but it was verified that the ratio of kinetic to internal energy remained less than 5%. An integration point per ply was included to represent the layup, and loading at 100 mm/s was applied. MAT_054 was used to represent the mechanical behaviour of the composite tubes due to its efficiency. Lastly, tie-break contacts were added to connect the two layers of shells. Huang and Wang noted a good correlation between the numerical and experimental results. Splaying of the tube under progressive crushing was well captured. However, significant, non-representative, local buckling was also observed in the numerical results. This was explained to be because the ply discount method cannot accurately represent the quasi-brittle failure of laminates resulting from various damage accumulations.

Kumar et al. [69] calibrated MAT_054 to simulate the axial crushing of 200 mm circular $[90_2/\pm 45_2/0_2]_s$ carbon fibre/epoxy tubes for applications in vehicle armour using approaches similar to the aforementioned studies (i.e., rigid loading plate with initial velocity, shell elements to represent the channel, and integration points per ply). The trigger was simplified into a row of reduced-thickness shell elements. An automatic contact was used to define the contact between the impacting plate and the composite with a coefficient of friction of 0.3. MAT_054 was calibrated via trial-and-error with the initial model underpredicting specific energy absorption by 57.95% and the final model underpredicting by 1.49%.

Feraboli et al. [32] predicted the quasi-static energy absorption of $[0/90]_{3s}$ semi-circular sinusoidal (i.e., corrugated) channels manufactured with carbon fibre/epoxy unidirectional prepreg tape by calibrating MAT_054 in LS-DYNA. Once again, the channel was modelled as fully integrated shell elements with reduced thickness shells to model the trigger, as well as an integration point per ply to represent the layup. However, Feraboli et al. also provided stress-strain curves in the fibre and matrix directions as inputs to MAT_054. Additionally, a load versus penetration curve was provided to define the contact algorithm. The loading plate was modelled with shells as a rigid material with the properties of steel, a mass of 154 grams, and an initial velocity of 3.81 m/s. From their sensitivity study of material and non-physical parameters, Feraboli et al. noted that the most influential parameter for determining the success of a simulation was the SOFT crush front parameter. However, they noted that there was no way of determining this value beforehand or from an

experiment. Next, it was concluded that DFAILC and the compressive strength, XC, were most influential in controlling element deletion. By varying these three parameters, it was determined that the experimental results could be replicated via multiple combinations of non-physical parameters. Additionally, their results showed that while a simulation model can be developed to successfully capture the experiments, the modelling strategy was not truly predictive.

Kazemian and Cherniaev [114] compared the modelling capabilities of MAT_054 and MAT_058 in LS-DYNA via simulation models developed to capture the response of UD-NCF carbon fibre/epoxy square tubes subject to quasi-static (0.5 mm/min) transverse compression. Two stacking sequences, $[0_4, 90_3]_s$ and $[90_5, 0_2]_s$, were considered, and corresponding experimental results were acquired for comparison. Layers of TSHELL elements were used to represent the plies of the channels, while the supports and impactor were represented as solid rigid bodies. The layers were connected via a (tiebreak) contact card with failure defined based on a bilinear traction-separation law. This enabled the modelling of delamination. To reduce computational time, the loading speed was increased to 2 m/s and only one quarter of the NCF tube was modelled with the appropriate symmetry boundary conditions applied. Noting that MAT_054 only predicts instant failure, it predicted a row of eroded elements at the end of crushing, where a crack was observed experimentally. In contrast, MAT_058 did not predict erosion at that same point and instead displayed elements damaged in the fibre direction (via history variables). Both models were noted to replicate the qualitative pattern of matrix cracking observed experimentally as well as adequately predict the force-displacement response of the specimen. Thus, Kazemian and Cherniaev [114] concluded that both material models were robust and able to predict transverse damage reasonably well.

Rabiee and Ghasemnejad [117] developed simulation models to capture the behaviour of $[\pm 45/0/90/0/90]_s$ glass fibre/epoxy tubes axially loaded at quasi-static (2 mm/second) and impact (4 m/s to 8 m/s) rates in LS-DYNA. Rabiee and Ghasemnejad compared the quality of results between single and multi-layer shell representations of the tubes. Additionally, they considered different material models (i.e., MAT_054, MAT_058, MAT_059), element formulations (i.e., Hughes-Liu, fully integrated, Belytschko-Tsay, etc.), mesh sizes (0.5 mm to 5.5 mm), trigger representations (i.e., single or double-shell representations), contact definitions, and the inclusion or exclusion of delamination (i.e., contact with failure definitions between layers of shells). The purpose was to develop a finite element model capable of predicting energy absorbing capacity within 5% of the

corresponding experimental studies. Rabiee and Ghasemnejad verified that delamination could be appropriately modelled between the layers using an energy-based approach with the tiebreak option in contact cards. MAT_054 was ultimately selected to represent each ply, and while calibration exhibited good agreement with experimental results, it was noted to be sensitive to minimal changes in input. Overall, each material model was noted to possess a specific range for which it was valid. Newaz et al. [115] also achieved agreeable numerical and experimental results with the simulation model they developed in LS-DYNA using MAT_054 to model the dynamic axial loading of unidirectional carbon fibre/epoxy tubes with and without holes introduced along the specimen. Lastly, Samuel et al. [116] calibrated MAT_054 to predict the experimentally observed response of carbon fibre/epoxy tubes dynamically loaded and concluded that the parameters selected could be further used to simulate the crush response of various unidirectional carbon tubes under dynamic impact.

Zhou et al. [95] developed a numerical model in LS-DYNA to replicate the experimentally measured energy absorption of $[0/90/0_3]_s$ and $[0/\pm 60]_{2s}$ unidirectional carbon fibre/epoxy hat channels subject to dynamic three-point bending at impact velocities between 3.7 m/s and 6.5 m/s. The channels were modelled with a single layer of 4 mm shell elements, whereby one integration point was included per ply through the thickness of the shell. MAT54 was selected to represent the CFRPs and calibrated to the experimental results. After calibration, good agreement between the experimental and numerical results was observed for both stacking sequences. Where deviations existed, namely for the $[0/90/0_3]_s$ stacking sequence, Zhou et al. highlighted that it was due to the lack of consideration for modelling delamination.

2.6 Energy Absorption Capabilities of Adhesively Joined FRP Composite Parts

It is clear from the preceding review of the literature that the energy absorption capacity of closed-profile FRP composite channels under axial compression loading has been widely studied. However, tubes with closed profiles cannot be readily fabricated using some FRP composite manufacturing processes, including RTM. Instead, a closed section can be formed by adhesively joining two open channels and the resulting impact performance assessed. Adhesive bonding is one of the most common methods of joining FRP composites and boasts several advantages in comparison to mechanically fastened joints for structural applications [41], [118]. These advantages include a minimal impact on the overall structural weight, negligible stress concentrations in the bonded parts, high damping properties, and cost-effectiveness. However, mating surfaces require additional

preparation steps prior to bonding, and several adhesives require curing at elevated temperatures [119]. Moreover, modelling of adhesively bonded parts requires fracture characterization of the joint, as well as the implementation of additional contacts or cohesive elements to represent the bond.

2.6.1 Fracture Characterization of Adhesively Bonded Joints

Fracture mechanics principles are widely used to characterize the fracture of adhesively bonded FRP composite joints under different modes of deformation, including opening mode (Mode I) and sliding mode (Mode II) [120]. Corresponding fracture criteria based on the strain energy release rate (SERR), G , are most commonly utilized because the fracture toughness or critical SERR, G_c , are readily characterized from fracture experiments [120]. To obtain the critical SERR for Mode I fracture (G_{IC}), double cantilever beam (DCB) tests are widely employed. DCB tests for adhesively bonded FRP composites typically follow the standardized test ASTM D5528-0, although this test was originally developed to characterize Mode I interlaminar fracture toughness [121] [34]. The DCB specimen consists of two identical adherends bonded with an adhesive (Figure 2.29). A small portion at one end of the specimen is left unbonded and tested in tension via loading blocks bonded to the cracked end of the specimen. Loading is applied via displacement control. The crack growth is monitored during the test to enable the determination of the SERR. Data reduction methods are available for determining G_{IC} .

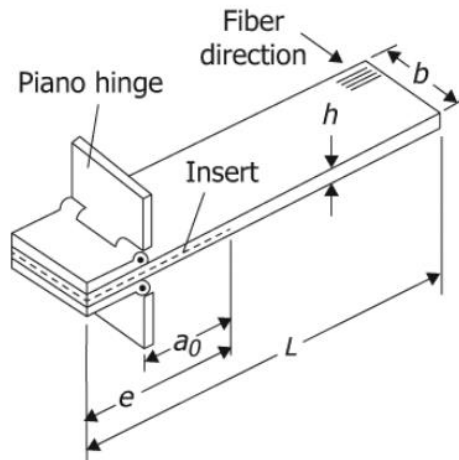


Figure 2.29: (a) Typical DCB test geometry; where a_0 is the initial crack length (distance between the application of the load and the pre-implanted insert), h is thickness of the DCB

specimen, e is total insert length, L is the length of the specimen, and b is the width of the DCB specimen Adapted from [121].

SERR, G_I , is defined as [121]:

$$G_I = -\frac{1}{b} \frac{dU}{da} \quad (30)$$

where U is the total elastic strain energy in the test specimen, b is the specimen width, and a is the crack length. Mode I fracture toughness is then calculated using either the modified beam theory (MBT) or the compliance calibration method [120], [122]. The strain energy release rate for a double cantilever beam specimen based on MBT is given as:

$$G_I = \frac{3P\delta}{2ba} \quad (31)$$

where P is the applied load and δ is the load point displacement. However, MBT may overestimate G_I due to possible rotation at the crack tip. To correct for this possible source of error, a slightly longer crack length, $a + |\Delta|$, is used. This may be determined experimentally, where Δ is the effective crack extension to correct for the rotation of DCB arms at the delamination front. Therefore, the strain energy release rate is expressed as [123]:

$$G_I = \frac{3P\delta}{2b(a + |\Delta|)} \quad (32)$$

Characterization of the Mode II shearing interlaminar fracture toughness, G_{IIc} , is most commonly achieved via end notch flexure (ENF) tests per ASTM D7905 [123], [34]. A beam is placed on top of two supporting rollers, and a bending load is applied via a roller at the beam's center (Figure 2.30). The initial crack is aligned to fall between the loading and supporting rollers. The shear forces acting on the longitudinal joining plane result in longitudinal sliding of the crack faces (Mode II). While several data reduction approaches have been proposed for the ENF test, the most common measure of the energy release rate is via the compliance calibration method [124], [34].

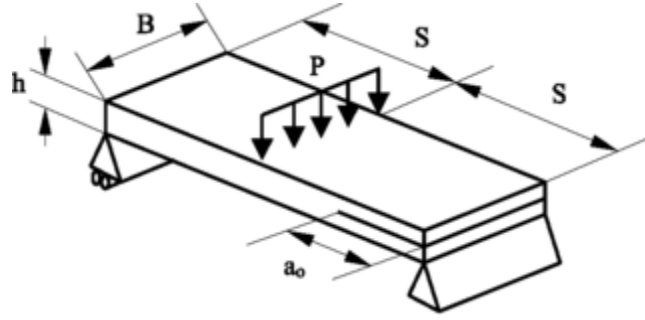


Figure 2.30: End notch flexure (ENF) test geometry for Mode-II interlaminar toughness testing, adapted from [34].

Within the ASTM standard, a procedure is outlined to obtain the non-pre-cracked toughness and pre-cracked toughness from the same specimen. Compliance calibration tests are performed by loading the specimen to the peak compliance calibration force (50% of the expected value of the critical force at that particular crack length) and then unloading [34]. The force and deflection data are recorded continuously, or at frequent and regular intervals during the loading portion. Non-pre-cracked and pre-cracked initiation values of G_{IIC} are obtained from the maximum force. With this method, the Mode II energy release rate is [45] given as:

$$G_{IIC} = \frac{9P^2 a^2}{16P^2 E h^3} \quad (33)$$

2.6.2 Adhesive Modelling in LS-DYNA

Tie breaks or cohesive elements are frequently used to predict fracture of adhesive joints in LS-DYNA. A brief review of these approaches follow.

2.6.2.1 Tiebreaks

Tiebreak contacts are a numerical method of joining parts within a commercial finite element solver, whereby a failure criterion is introduced to release nodes tied together based on force, displacement, or energy criteria [125]. They enable the modelling of adhesive fracture, where both compressive and tensile forces can be transmitted [96]. Notable inputs are normal and shear failure stresses, while outputs are contact stresses and nodal forces (contact stresses multiplied by the area of the shell element or the face of a solid element) [99]. Automatic tiebreak contacts are recommended because they allow for stress-based failure criterion, automatic shell normal orientation, consideration of shell thickness offsets and options for damage (i.e.,

Contact_Automatic_One_Way_Surface_To_Surface_Tiebreak). This contact with Option 11 would be analogous to MAT_138 used with shells, while Option 8 would be analogous to MAT_138 used with solids [99].

2.6.2.2 Cohesive Zone Modelling

Cohesive zone modelling (CZM) is a method of simulating fracture whereby the stress state of the material ahead of a crack tip can be modelled as cohesive tractions resisting crack growth, or the separation of crack surfaces, across a cohesive process zone (Figure 2.31) [99], [125], [126]. As such, a traction-separation (also known as cohesive) law is defined to describe the relationship between traction (force per area) and separation (displacement) at a cohesive interface [120]. Bilinear (triangular) cohesive laws are most commonly used due to their simplicity in representing the fracture of adhesively bonded joints, but can also be considered a particular case of trilinear (trapezoidal) laws (Figure 2.32) [127]. Bilinear cohesive laws perform well for brittle or moderately ductile adhesives, while trilinear laws are preferable when adhesives are highly ductile. Additional cohesive laws include linear-parabolic, polynomial, or exponential. The experimentally derived critical SERR of the adhesive is required to calibrate a cohesive law and represents the area under the traction-separation curve. Additional parameters required to calibrate cohesive laws include initial stiffness and peak traction. Inverse methods are often used to determine these additional parameters, which involve iteratively curve fitting between experimentally measured force-displacement data and the response predicted by the representative finite element model [120].

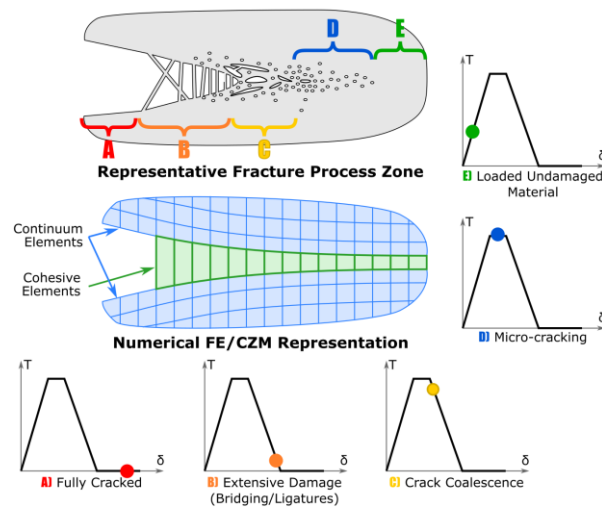


Figure 2.31: Representative schematic of the fracture process zone of a crack and how it is represented using cohesive elements [125]. Regions of the fracture process zone are mapped to a trapezoidal traction-separation response.

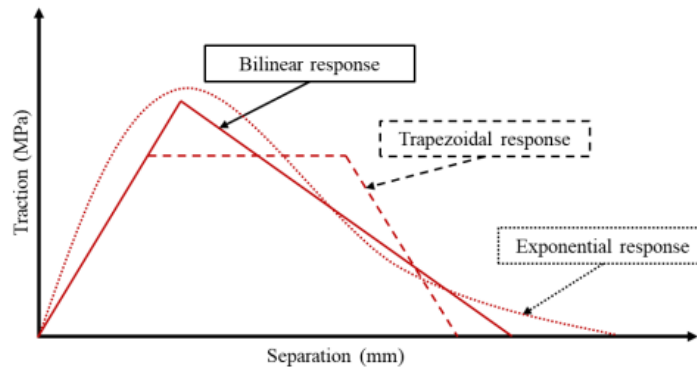


Figure 2.32: Common traction-separation laws defined for cohesive zone models capturing the fracture of adhesively bonded joints [120].

Cohesive zone modelling is often implemented in finite element software via cohesive elements. Cohesive elements are finite or zero-thickness elements made of shells, TSHELLS, or solid elements connecting planes or three-dimensional solids [99], [127]. The elements themselves consist of three non-linear springs (one in the normal direction and two in the shear direction) to relate the relative displacement (separation) between the upper and lower surfaces to a force per unit area (traction) (Figure 2.33) [128]. A single layer of cohesive elements provides the representative response of the entire adhesive layer, along a predefined fracture path. A major advantage of cohesive elements is that they can simulate both crack initiation and propagation [129]. Additionally, they allow one to account for compliance in the connection and damage in the material. Lastly, cohesive elements are beneficial, especially when interface strengths are relatively weak in comparison to the adjoining materials [130].

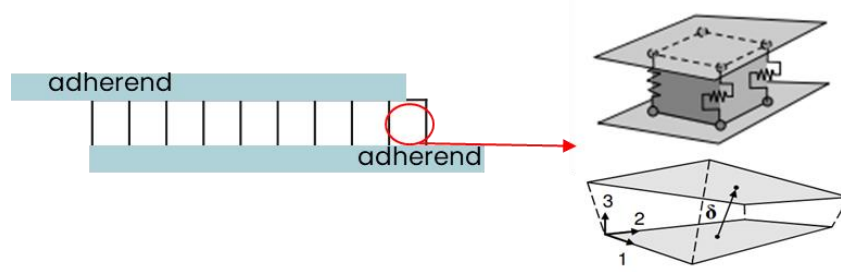


Figure 2.33: Conceptual representation of a cohesive element for the implementation of cohesive zone modelling, where δ is relative displacement, adapted from [128].

2.6.2.3 Available Cohesive Material Models in LS-DYNA

There are two types of cohesive elements available in LS-DYNA: type 19 and type 20 [96], [105]. Both support zero-thickness elements. Type 19 connects volume elements (moments are not transferred) while Type 20 elements have offsets for use with shells [99]. In this case, the cohesive element is assumed to be centred between two layers of shells and moments are applied to the shells. The elements output three tractions with units of force/per unit area: in-plane shear, orthogonal in-plane shear stress, and traction in the normal direction (normal stress) [105].

Cohesive zone models have been used to model the failure of adhesively bonded joints [131] as well as delamination between plies in laminates [102]. The available cohesive zone models are differentiated based on their traction-separation laws, with some offering strain-rate dependency and temperature dependency [34].

Table 2.2: Cohesive zone material models available in LS-DYNA [99].

Material Model	Traction - Separation Law/ Shape	Inputs	Comments
MAT_138: Cohesive_Mixed_Mode	Bilinear (linear softening) with mixed mode delamination criterion	T, S, G_{IC} , G_{IIC} , E_T , E_N , δ_T , δ_S , XMU (mixed mode exponent), ultimate displacements in tangential and normal directions	N/A
MAT184: Cohesive_Elastic	Linear Elastic	T, S, E_T , E_S , ultimate displacements in tangential and normal directions	N/A

MAT185: Cohesive_Elastic_TH	Tri-linear	Peak traction, ultimate displacements in tangential and normal directions, scaled distances to peak traction, softening, and failure	Reversible loading and unloading path
MAT_186: Cohesive_General	Arbitrary normalized traction-separation law given by load curve	Type of formulation, normalized traction-separation laws, T, S, G_{IC} , G_{IIC} XMU	Irreversible damage with unloading and reloading paths pointing to and from the origin
MAT_240: Cohesive_Mixed_Mode_Elasto_Plastic_Rate	Tri-linear	T, S, G_{IC} , G_{IIC} , E, G, strain-rate dependency, f_{G1} , f_{G2}	Considers rate and plasticity effects Ideally plastic behaviour; different loading and unloading paths cannot model brittle fracture

2.6.3 Studies on Modelling the Fracture of Adhesively Bonded Composites

Very limited studies are available on experimental or numerical investigations into energy-absorbing capacity of adhesively bonded composites. To begin, Han et al. [132] quasi-statically tested (5 mm/min) and numerically captured the fracture behaviour of $[\pm 45/(0/90)_4/0/\pm 45]$ carbon fibre/epoxy hat channels bonded to flat plates via the structural adhesive, SikaPower 4720 (Figure 2.34a). An initial crack length and chamfer at the end of the bonded channels were implemented to improve the fracture initiation path and progressive crushing. During the experimental results, it was observed that the outer layers of the channels curved outwards while the inner layers bent inwards (Figure 2.35a). No cracks were observed within the adhesive layer. For the simulation, two layers of shells elements were used to better capture the bevel trigger, improve representation of initial fracture path, and capture the interlaminar behaviour of the channels (Figure 2.34b). A four-node element with bending and membrane capabilities (Shell 163) was selected and MAT_054 was used to represent the failure of the CFRP. A rigid wall was used to represent the applied crushing load at a constant velocity of 2000 mm/s. Tiebreak contacts were added between the two CFRP layers to represent interlaminar resin as well as added to represent the structural adhesive connecting the hat section and flat plate. The interfacial failure in the adhesive layer and composite matrix were well predicted (Figure 2.35b). Additionally, good agreement between the measured and predicted response was observed in terms of peak load and energy absorption with the highest deviation being 6% for peak force (Figure 2.35c).

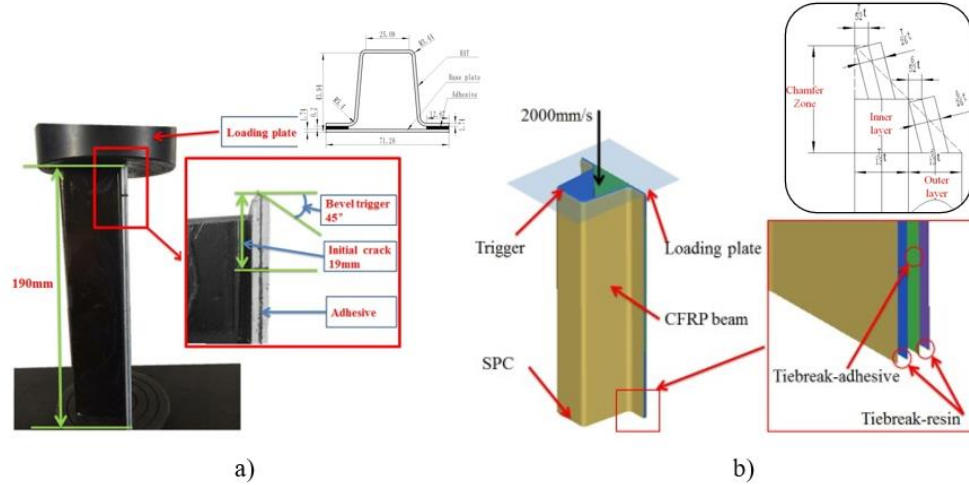


Figure 2.34: (a) Experimental set-up (b) and numerical representations of bonded hat channel and flat plate quasi-statically tested by Han et al., adapted from [132].

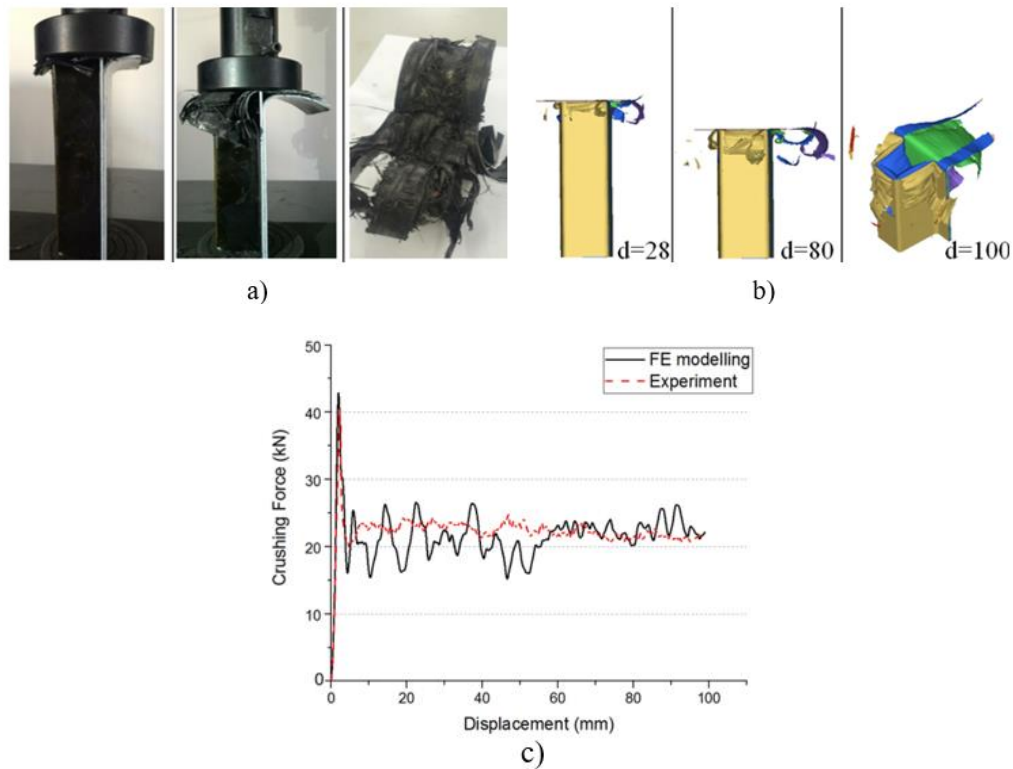


Figure 2.35: Representative (a) experimental and (b) numerical failure of bonded hat channel and flat plate tested quasi-statically by Han et al., adapted from [132]. (c) Experimental and predicted (numerical) force-displacement response.

2.7 Gaps in Literature and Research Objectives

Non-crimp fabric composites, especially those manufactured via HP-RTM, possess great potential for addressing lightweighting and manufacturing challenges, as has been demonstrated by the use of composites by luxury car manufacturers (e.g., BMW and Lamborghini). However, studies investigating the EAC of composite structures for occupant protection are limited to closed-profile channels (tubes) and often do not consider open-profile channels. Where studies exist, it has been demonstrated that insensitivity to changes in loading conditions observed with tubes may not exist with open channels (i.e., [86]). Therefore, further exploration of the performance of open channels is required. Beyond this, there is a lack of published research comprehensively evaluating the energy-absorbing capacity of composite structures fabricated with UD-NCFs, reinforcements which offer more affordable and higher volume production of CFRPs. Thus far, most studies are focused on unidirectional tape or woven carbon fibre fabrics. Lastly, while there are numerous studies which consider displacement-controlled quasi-static loading, few studies replicate dynamic crash conditions and are often limited to energy-controlled drop-testing. This limits the evaluation of the strain rate effects on the performance of composite channels. Among the dynamic studies available, notable decreases in energy absorption have been recorded at higher strain rates in comparison to lower rates. Substantiating the application of UD-NCF composite structures as energy absorbers in vehicles requires additional investigation into the performance of open and closed-profile channels subject to dynamic velocity-controlled loading with changes in geometry and stacking sequence.

Adoption of UD-NCF CFRP composites into automotive structures is also limited by the reliability of the material models available to simulate them in commercial finite element software packages. In an attempt to address this, many finite element simulation models have been developed within LS-DYNA, specifically with MAT_054, to replicate the energy-absorbing behaviour of composite structures observed experimentally. As with the experimental studies, many of the numerical investigations were limited to closed profile channels fabricated with woven and unidirectional fabrics. They demonstrated that MAT_054 can be calibrated to a set of conditions with minimal discrepancy between the experimental and numerical results and then recommend that the calibrated values be used to predict the performance of different geometries, loading conditions, or stacking sequences. However, there is a lack of evaluation as to how such changes may affect the predictive capability of the material model and the validity of the overall simulation models. To use FEM as an effective design tool for composite materials, it is necessary to investigate the fidelity of the material

model for changes in design decisions and note significant limitations, should any exist. Thus, there is a need to investigate the robustness of MAT_054 in predicting the performance of new conditions after the material model has been calibrated.

Lastly, there are very few studies which have considered the energy absorption capacity of bonded composite structures, let alone bonded UD-NCF CFRPs, despite the advantage they offer in simplifying the manufacturing and assembly of composite structures into larger vehicle bodies. However, by introducing additional processing steps and changes in geometry, the energy-absorbing capacity of the finished component risks being limited and thus requires study. Additionally, the robustness of MAT_054 can be further evaluated through the development of a simulation model which considers this increasingly complex condition and additional contacts to represent bonding.

The goal of this study is to support the development of a high-fidelity impact simulation model for predicting the energy absorption capacity of UD-NCF carbon fibre-reinforced epoxy composite structures. The specific research objectives are detailed as follows:

1. Characterize the energy-absorption capacity and failure behaviour of single UD-NCF carbon fibre-reinforced snap-cure epoxy channels manufactured via HP-RTM when subject to dynamic and quasi-static axial compressive loading. Assess the influence of stacking sequence, geometry, and loading conditions on the performance of the channels.
2. Develop a set of simulation models in LS-DYNA to predict the performance of composite channels with different stacking sequences, geometries, and loading conditions to investigate the robustness of the available material model, MAT_054, after its calibration to experimental data from a baseline condition.
3. Experimentally characterize the energy-absorbing capacity and failure behaviour of adhesively bonded double-hat UD-NCF carbon/epoxy channels for comparison to the single channels. Expand the simulation models developed for single channels for the double-hat channels through the inclusion of cohesive elements and further evaluate the robustness of MAT_054.

Chapter 3

Experimental Characterization Methodology

The process used to manufacture the experimentally tested channels is presented in this chapter. Tapered hat and double-hat channels were fabricated to complement the previously reviewed hat and corrugated channel results (Section 2.4.1.2). The increasingly complex geometries provided the opportunity to investigate the possibility of more complex failure processes during loading. They also provided additional data for the validation of the simulation models developed (Chapter 4 and Chapter 6). Additionally, the experimental setups for the dynamic and quasi-static axial loading tests are presented.

3.1 Channel Production

3.1.1 Channel Geometries

Two types of channels were manufactured and machined for experimental testing: tapered hat channels (Figure 3.1) and double-hat channels (Figure 3.2). The tapered channels initially maintained a similar geometry to the hat channel for the first 200 mm and then the cross-sections increased in scale through the length of the remaining 600 mm of the channel to create a tapered section. The tapered-hat channels can be considered analogous to the S-rails in an automotive underbody, which are significant energy-absorbing members [133], [134], [135], [136]. These members are often designed with an increasing cross-section along the length of the component (Figure 3.3). The double hat channels consist of two hat channels bonded together with an adhesive. For both the hat channel and tapered hat channels, $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ stacking sequences were manufactured, while for the latter geometry, an additional $[0/90/\pm 45]_s$ stacking sequence was also manufactured. In all cases, the channels were composed of eight plies with an average thickness of 2.7 mm.

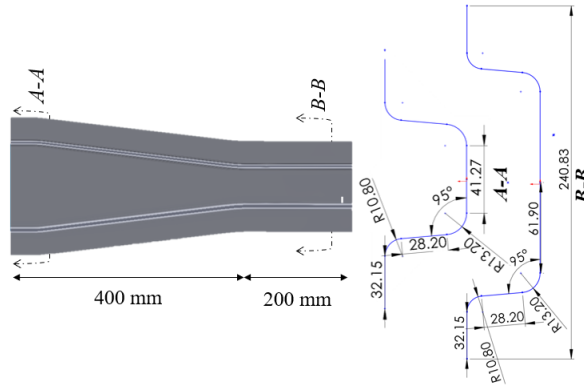


Figure 3.1: Geometry and dimensions of tapered-hat channel manufactured for experimental testing.

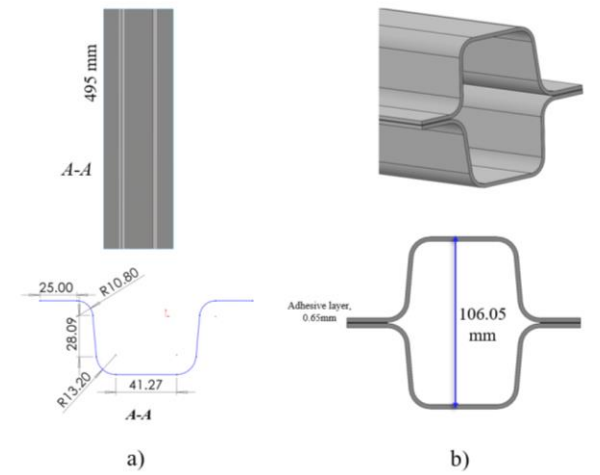


Figure 3.2: Geometry and dimensions of (a) an individual and (b) two hat channels bonded to form a double-hat channel.

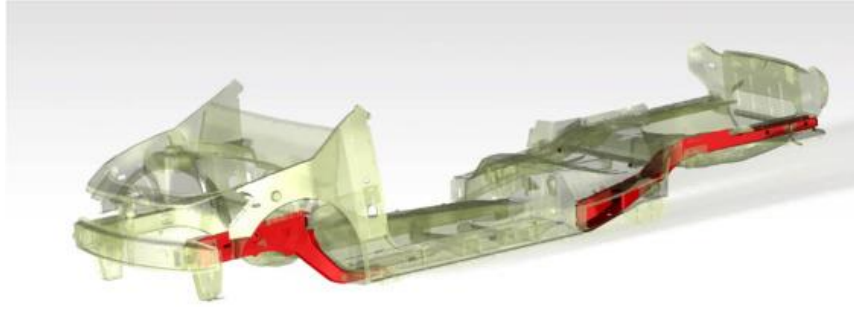


Figure 3.3: Examples of front and rear S-rails in automotive underbody, highlighted in red [133].

3.1.2 Materials & Manufacturing

The reinforcing phase used for the channels was the commercially available Zoltek™ PX35-UD300 stitch-bonded unidirectional non-crimp fabric [137] (Appendix B). This heavy-tow fabric was made of five-millimetre-wide aligned tows, each consisting of 50,000 continuous PX35 carbon fibre filaments, supported by perpendicular glass fibres and stitched together with polyester yarn in a tricot pattern in the tow direction (Figure 3.4) [43], [47]. A thermosetting binder powder was uniformly distributed on the stitching side of the fabric. The areal density of the fabric was $333 \frac{g}{m^2}$ with carbon fibre tows accounting for 92.8% of the total weight [43].

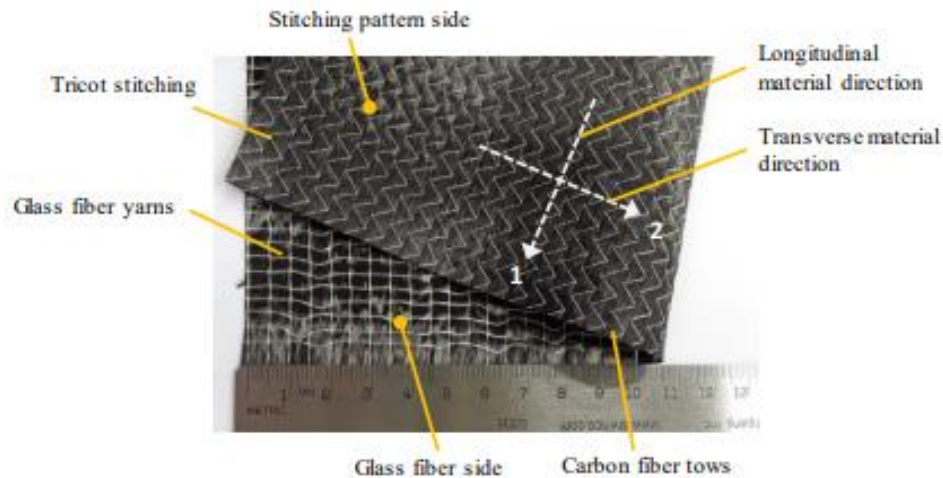


Figure 3.4: Composition of the Zoltek™ PX35-UD300 unidirectional non-crimp fabric [137], [43].

The matrix consisted of a three-part resin system: EPIKOTE™ Resin TRAC 06150 snap-cure epoxy resin, EPIKURE™ Curing Agent TRAC 06150, and HELOXY™ Additive TRAC 06805 mould release [54]. This system was designed by Westlake Epoxy (formerly Hexion Inc.) for the mass-production of automotive structural parts and features a curing time of 5 minutes [54], [112], [138]. The resin, curing agent, and mould release agent were mixed at a ratio of 100:24:1.5, respectively [138], [139].

The hat and tapered hat channels were fabricated using an HP-RTM process [112], [139]. The moulds for the components were designed with full fibre clamping and featured a centre-fed injection inlet. The UD-NCF fabric layers were cut from a roll, stacked, and placed onto a preheated mould (120°C) which was mounted within a 25,000 kN Dieffenbacher press. The mould was closed to preform the fabric layers and a vacuum was applied for 1 minute prior to injecting the resin mixture. A metering unit was used to control the mixing ratio of the resin mixture, which was rapidly mixed and injected into the mould through a mixing head at a pressure of 120 bars and a flow rate of 40 g/s. The resin was allowed to cure in the mould for 5 minutes prior to the demoulding of the part [139].

In previous work by Suratkar [140], specimens extracted from flat plates of the same UD-NCF carbon fibre/epoxy system were tested under quasi-static loading conditions to characterize ply-level mechanical properties (Table 1). These properties were inputted into the material models used in the simulations created for this study (Chapter 4).

Table 3.1: Ply properties of non-crimp carbon fibre-epoxy composite under quasi-static loading for a unidirectional lamina with the stacking sequence, [0]₇/ [0]₁₁, and a fibre volume fraction of 53%.

Material Property	Mean Value (± standard deviation)
Density [$\frac{g}{cm^3}$] [37]	1.46
Longitudinal Young's Modulus, E_1 , [GPa] [140]	121 (±7)
Transverse Young's Modulus, E_2 , [GPa] [140]	8 (±2)
Major In-Plane Poisson's Ratio, ν_{12} [140]	0.37 (±0.01)
In-Plane Shear Modulus, G_{12} [GPa] [140]	3.55 (± 0.14)
Longitudinal Tensile Strength, X_t , [MPa] [140]	1637 (±168)

Transverse Tensile Strength, Y_t , [MPa] [140]	60 (± 4)
Longitudinal Compressive Strength, X_c , [MPa] [140]	1001 (± 97)
Transverse Compressive Strength, Y_c , [MPa] [140]	145 (± 4)
In-Place Shear Strength, S_L , [MPa] [140]	90 (± 2)
Fibre Longitudinal Strain at Failure ϵ_{1f} [%] [140]	0.66 (± 0.07)
Fibre Transverse strain at failure ϵ_{2f} [%] [140]	0.66 (± 0.07)
Composite Longitudinal Strain at Failure ϵ_{1c} [%] [140]	0.8 (± 0.09)
Composite Transverse Strain at Failure ϵ_{2c} [%] [140]	2.42 (± 0.33)
In-Plane Shear Strain at Failure γ_{12f} [%] [140]	8.88 (± 1.9)

3.1.3 Specimen Preparation

The tapered hat channels were trimmed to size with a bandsaw, or a DX-350 N tile saw (Rubi). Subsequently, a trigger for progressive crushing was added to one end of each channel by machining a 45° chamfer 2.5 mm to 3 mm in depth (Figure 3.5). Impact loading of the channels began at this end.

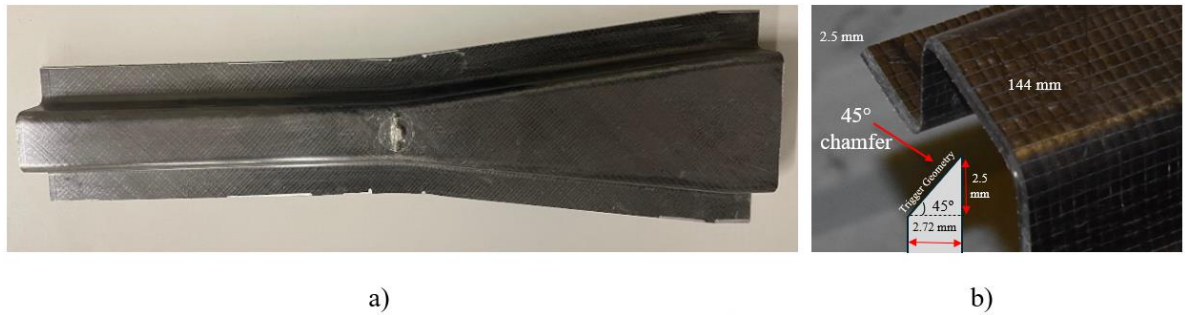


Figure 3.5: (a) Manufactured, trimmed tapered channel and (b) 45° chamfer machined at the impacted end of the channel.

To fabricate the double-hat channels, a pair of untrimmed hat channels, originally 800 mm long, were bonded together to form a double hat channel using the impact-resistant structural adhesive, 3M 07333 [141]. The properties of the adhesive are summarized in Table 3.2. Several bonding processes were tried until successful bonding was achieved. Successful bonding was defined by symmetry between the two channels across the direction perpendicular to the axial direction of the specimen and a microscopically verifiable consistent bond line thickness (Figure 3.6). The

recommended process that led to the greatest number of successfully bonded channels is described below.

Table 3.2: Properties of Impact Resistant Structural Adhesive 3M 07333 [141].

Material Property	Value
Density [$\frac{g}{cm^3}$]	1.14
Elastic Modulus [GPa]	2.1
Poisson's Ratio	0.43 [120]

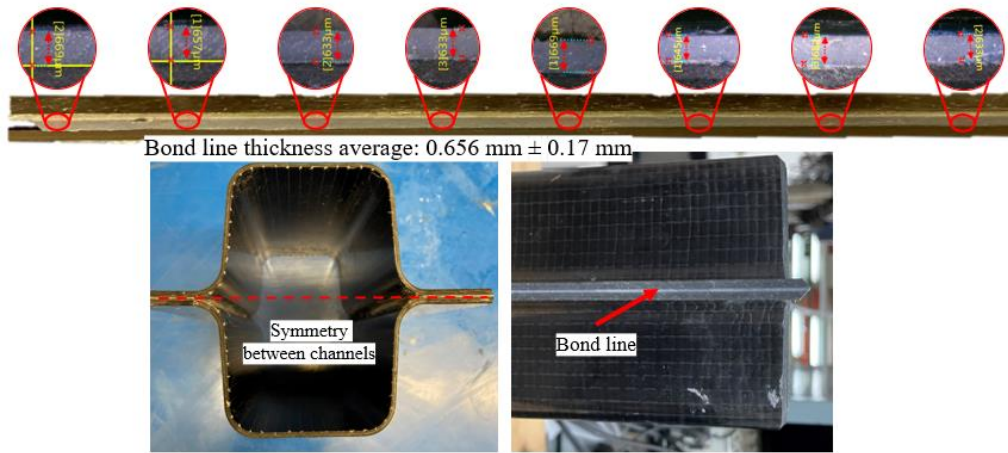


Figure 3.6: Examples of indicators of a successfully bonded double-hat channel: microscopically verifiable consistent bond line thickness and symmetry between channels.

In accordance with the surface preparation procedure described by Dehaghani [120], the flanges of each channel were cleaned with acetone and then sanded by hand. To avoid premature debonding, sanding was completed with 240-grit sandpaper until an approximate surface roughness of $0.75\mu m$ (R_a) was achieved along the length of each flange. The flanges were then measured, and excess flange width was segregated through the application of flash tape until the width of the flange exposed for adhesive application was 25 millimetres (Figure 3.7). Excess flange width was retained to provide an area for adhering shims to control the bond line thickness.

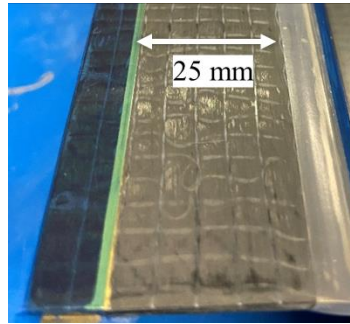


Figure 3.7: Example of flange of a hat channel sanded and with flash tape applied to indicate a final width of 25mm.

A target bond line thickness of 0.65 millimetres was set to maximize the strength of the bond. This thickness was adopted because proportional increases in bond strength and fracture toughness with increasing bond line thickness up to 0.65 mm were demonstrated by Dehaghani [120] for a similar methodology. Additionally, Dehaghani calibrated a bilinear traction separation law in ABAQUS at 0.65 mm [120] and Watson [142] calibrated a trilinear traction-separation law in LS-DYNA for a bond line thickness of 0.64 mm, providing parameters for modelling the adhesive in simulations (Chapter 4). To maintain a 0.65 mm bond line thickness across the channels, 0.65 mm shims were manufactured (Figure 3.8) and treated with LOCTITE FREKOTE mould release as per manufacturing instructions [143]. After the surfaces were once again cleaned with acetone, the shims were taped onto the channel flanges, and the adhesive was applied on both flanges of both channels (Figure 3.8).

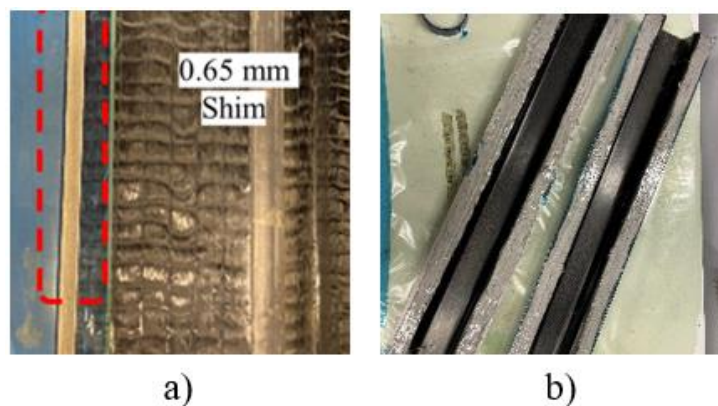


Figure 3.8: (a) Shims added to the excess flange width of a hat channel and (b) adhesive applied to the flanges of a pair of channels (right).

Fixtures with a profile corresponding to the outside surface of the hat channels were 3D printed out of ABS plastic and attached to the channels prior to bonding to improve the alignment (symmetry as per Figure 3.6) of one channel with respect to the other (Figure 3.9). These fixtures were also treated with LOCTITE FREKOTE and placed intermittently along the length of the channels. Two-inch binder clamps were then added around the fixtures to maintain the aligned orientation as well as bond line thickness. Lastly, the channels were placed in a 1600 series HAFO oven, and the adhesive was cured according to the manufacturer's instructions (30 minutes at 80°C [141]).

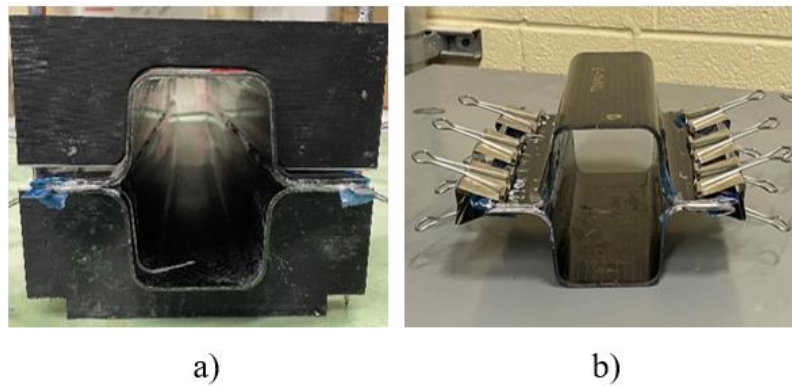


Figure 3.9: (a) Fixtures 3D printed to align hat channels to one another and (b) Binder clips applied around the flange of the channel to maintain bond line thickness and alignment (right).

After curing, the shims and excess flange width on each side of the channels were trimmed using a DX-350 N tile saw (Rubi) to achieve the cross-section dimensions as designed. The channels were also cut to approximately 190 mm in length prior to testing. Lastly, as was the case for the open channels, a 45° chamfer, 2.5 mm in length was machined at what would be the impacted end of the channels to serve as the progressive crushing initiator (trigger). The other end of the channel was machined level. An example of a completed double-hat channel is shown in Figure 3.10.

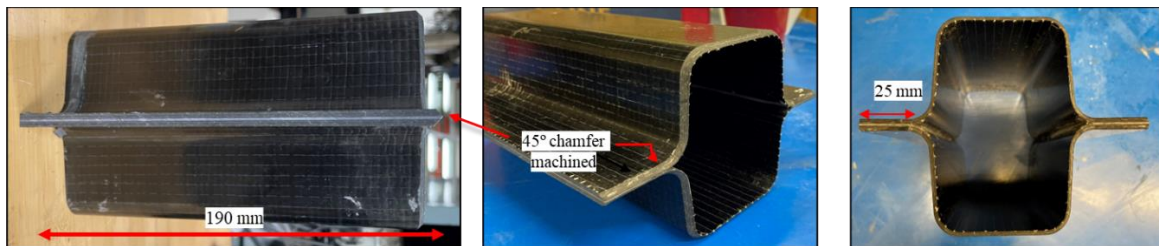


Figure 3.10: Representative double-hat channel component post-bonding and machining.

3.2 Experimental Set-Up of Axial Crushing

3.2.1 Dynamic Axial Loading

Dynamic axial loading of the tapered hat channels and double-hat channels was completed using a Seattle Safety Impact Sled [144] (Figure 3.13). Via high-strength rope, the sled was connected to a piston driven by compressed air [144]. A nominal velocity was inputted, and the actual velocity was calculated by a trigger system, whereby the amount of time it took for the sled to travel a 13-inch trigger gate and laser switch represented the initial velocity. The trigger system also simultaneously initiated the data acquisition system and high-speed cameras. The impact sled is rated for a maximum impact energy of 168,500 Joules and a maximum impact load of 800 kN. The channels were mounted to a stationary vertical wall fixture. To measure the force during impact, three 120 kN Kistler Quartz Force Link load cells were mounted behind the vertical wall. Two MSI accelerometers were attached to the sled to record the deceleration during impact. All data was recorded using a g-hardened on-board DTS Slice data acquisition system at a sampling rate of 10,000 Hz. The acceleration data was filtered with an SAE CFC 60 (100 Hz low pass) filter [76] and then double-integrated with the trapezoidal rule to provide displacement during loading. This method has been previously validated with a laser positioning system (Keyence LK-G507).

50 mm thick steel fixtures, conforming to the outside profile of the appropriate channel geometry, were used to fix one end of the channel component to the wall (Figure 3.11 and Figure 3.12). Details on the geometry and dimensions of the manufactured fixtures are provided in Appendix C. The sled, weighing a total of 858 kg, was translated in the axial direction of the channels with an initial impact speed of $7500 \frac{mm}{s}$. The sled crushed the tapered hat channels over a distance of approximately 300 mm and crushed the double-hat channels over a distance of approximately 90 mm. It was then decelerated to a stop via subsequent impact with aluminum honeycomb cubes attached on either side of the components. The displacement of the sled between initially impacting the channels and impacting the aluminum cubes was defined as the *free crush distance*. The dimensions of the aluminum cubes varied with the initial length of the channels.

An enclosure made of slotted aluminum extrusions and plexiglass surrounded the channels to limit the spread of carbon fibre dust during loading (Figure 3.13). Similarly, a vacuum was inserted on both sides of the enclosure to capture dust as the channel was crushed and to improve visibility for the mounted video cameras. Three high-speed cameras were used to provide additional insight into

the behaviour of the samples during loading. One Photron AX100 high-speed camera was positioned to capture video on the side of a component (i.e. along the bond line in the case of double-hat channels) and sampled at a rate of 10,000 frames per second (camera 2 in Figure 3.13). For the testing of the tapered hat channels, a Photron SAZ was positioned along the same side as the Photron AX100 to capture the crush process with greater temporal resolution at 100,000 frames per second (camera 3 in Figure 3.13). A second Photron AX100 camera was positioned over the channels to provide a bird's eye view and captured images at 10,000 frames per second (camera 1 in Figure 3.13). For the testing of the double-hat channels, the Photron SAZ was positioned on the other side of the channel so that capture crack propagation near the bond line along both sides of the channels could be captured. A representative set-up of the dynamic experiments is shown in Figure 3.13.

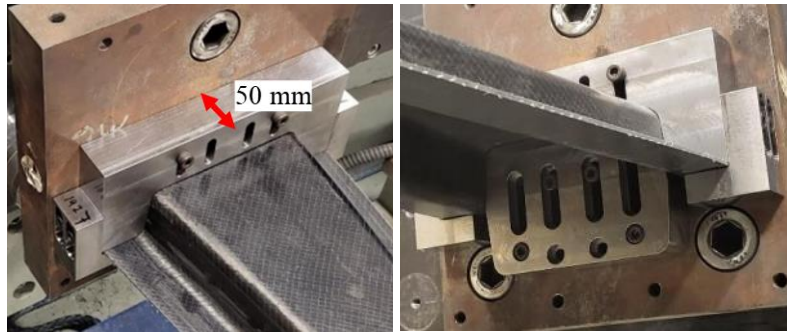


Figure 3.11: Steel fixture used during the dynamic axial testing of tapered hat channels to connect the channels to the wall [145].

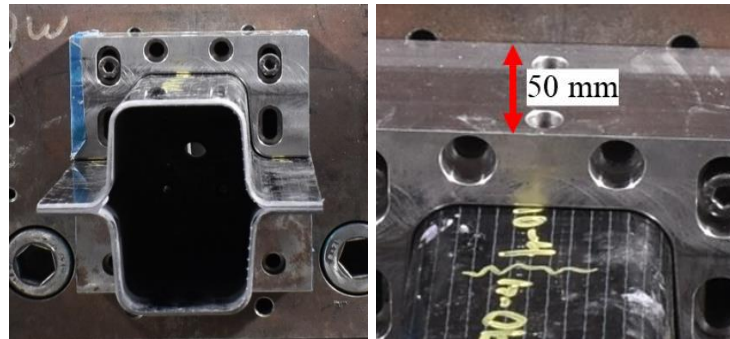


Figure 3.12: Steel fixtures used for both the dynamic and quasi-static testing of double-hat channels to connect the channels to the wall.

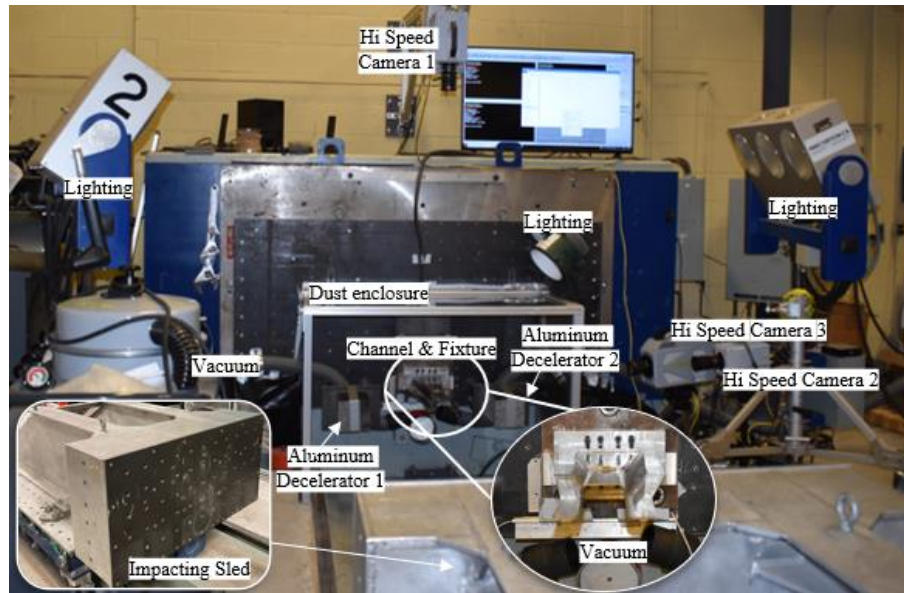


Figure 3.13: Representative setup for the experimental dynamic axial loading tests.

3.2.2 Quasi-static Axial Crushing

Quasi-static axial crushing of the double-hat channels was performed with a 120-ton Macrodyne Press. To begin, the same fixtures used for dynamic axial crushing (Figure 3.12) were attached to a raised platform and used to connect the channels to a fixed plate—analogous to the fixed vertical wall used for the dynamic loading. Also similar to the dynamic set-up, three cameras were used to capture the behaviour of the channel as it was crushed. Two cameras were mid-speed Point Gray Gazelle Camera-Links, with one on each side of the double-hat channel (focused on the bond line). Each camera captured images at a rate of forty frames per second. The third camera was a GoPro Max, oriented to capture the front view of the channel (analogous to the bird's eye view captured of the channels during dynamic loading) at a rate of thirty frames per second. Lastly, three Tovey SW20-50kip load cells, each with a capacity of 50,000 pounds, were attached behind a platen which approached and crushed the channel at a velocity of $1 \frac{mm}{s}$. Data was acquired at 0.04 to 0.05-second intervals. Displacement was monitored via feedback provided with a PID algorithm to control motion. The platen crushed the double hat channels over a distance of approximately 90 mm. A representative set-up of the quasi-static experiment is shown in Figure 3.14.

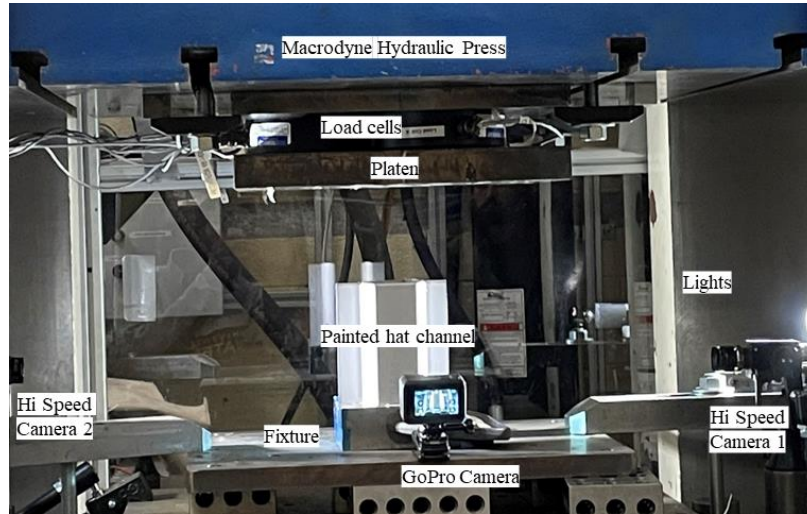


Figure 3.14: Representative setup for the experimental quasi-static axial loading tests.

3.2.3 Experimental Test Matrix

The experimental tests completed for the tapered hat channels (Table 3.3) and double-hat channels (Table 3.4) are summarized below. Quasi-static tests were not completed for the tapered hat channel because of its extended geometry. Quasi-static testing would have required the trimming of the channel to fit within the hydraulic press. However, because the cross-section was not constant, it would not have been possible to test the effect of geometric change along the length of the channel. Performance metrics were calculated for the results using the equations outlined in Table 2.1. For all metrics, the average of the trials and the standard deviation associated with each are presented in Chapter 5. Calculations of specific energy absorption are based on the amount of mass crushed during testing and do not include consideration of the uncrushed mass remaining at the end of the test.

Table 3.3: Quantity and type of experimental trials completed for the tapered hat channels.

	$[0/\pm 45/90]_s$	$[0/90/\pm 45]_s$	$[\pm 45/0_2]_s$
Dynamic Axial Crushing ($7500 \frac{mm}{s}$)	3	3	3

Table 3.4: Quantity and type of experimental trials completed for double-hat channels.

	$[0/\pm 45/90]_s$	$[\pm 45/0_2]_s$
Dynamic Axial Crushing ($7500 \frac{mm}{s}$)	3	3
Quasi-Static Axial Crushing ($1 \frac{mm}{s}$)	4	3

Chapter 4

Numerical Modelling Methodology

Details of the finite element models developed to simulate the experiments described in Chapter 3, as well as models developed to simulate the results of quasi-static and dynamic axial loading and three-point bending of corrugated and hat channels collected by Harvey [49], [53], are presented in this chapter. All geometries were created in SolidWorks 2024 (Dassault Systèmes, Vélizy-Villacoublay) followed by mesh generation in Hypermesh 2021.2 (Altair®, Chicago). The simulations were then developed and solved with the commercial explicit finite element software, LS-DYNA R11.1 (ANSYS, Pennsylvania), and the accompanying pre- and post-processor LS-Prepost R4.6.17 (ANSYS, Pennsylvania).

The composite material was defined via the material model, MAT_54: Enhanced_Composite_Damage, available in LS-DYNA. The non-physical parameters of the material model were first calibrated within an initial simulation model to replicate the average experimental results of $[0/\pm 45/90]_s$ hat channels axially loaded at an impact velocity of $7500 \frac{mm}{s}$ (Section 4.1). The calibrated material model was then used in the development of subsequent simulation models to predict the performance of other channel geometries, stacking sequences, and loading conditions for comparison to available experimental data. (Section 4.2). Lastly, simulation models were developed to predict the behaviour of double-hat channels axially loaded. This required the calibration of an additional material model, MAT_240, and the addition of cohesive elements to represent the behaviour of a structural adhesive bonding two hat channels together (Section 4.3).

4.1 Initial Simulation Model Development for the Baseline Condition: The Dynamic Axial Loading of $[0/\pm 45/90]_s$ Hat Channels

A finite element model was developed to simulate the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channels (i.e., baseline condition) experimentally tested by Harvey [76] (Figure 4.1). Within this simulation model, the non-physical parameters of MAT_054 were calibrated through parametric variation such that the force-displacement response and energy absorption predicted would coincide with the average experimental data. All subsequent simulation models maintained the same calibrated material model parameters despite changes in other conditions, which enabled evaluation of the robustness of MAT_054. The non-physical parameters selected for the simulation model of the

baseline condition are presented in Chapter 6 after the results of the parametric investigations. The following sections provide details on the development of the initial simulation model.

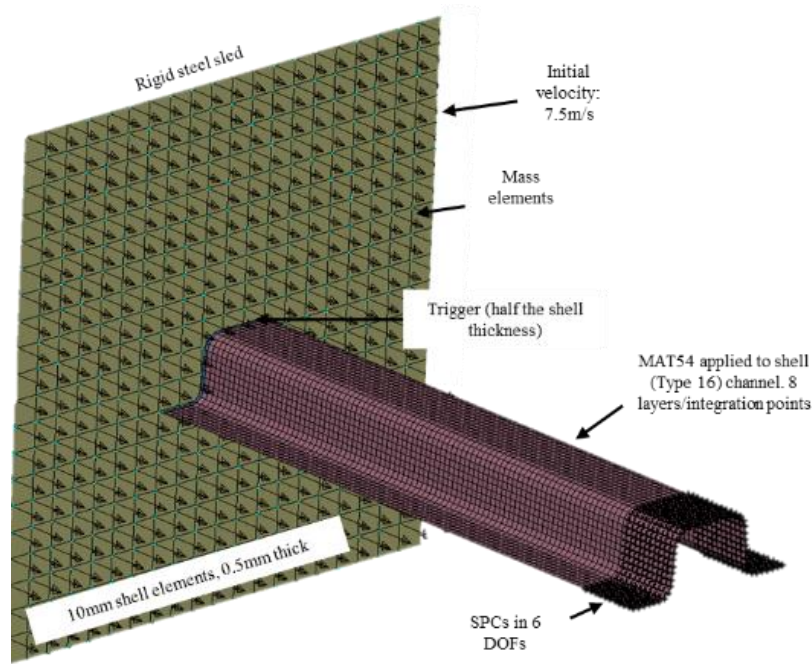


Figure 4.1: Finite element model representation of the dynamic axial loading of a $[0/\pm 45/90]_s$ hat channel.

4.1.1 Part Geometries & Mesh

A single-shell approach was employed to model the hat channel. This method added efficiency to the model at the cost of accuracy and predictive capability in modes where delamination or inter-ply interaction (i.e. friction or bending of fronds) at the crush front were predominant in the component's physical response [99]. In employing this approach, eight integration points (one per ply), with appropriately defined material directions and properties were assigned in PART_COMPOSITE. Each element was defined as a Type 16 fully integrated shell element with a thickness of 0.34 mm per ply (2.7 mm total thickness). This element type is regarded for its accuracy while being one of the least computationally expensive fully integrated elements [96]. Additionally, as recommended [146], hourglassing control for type 16 elements was applied to the channel and trigger parts (IHG = 8) to add warping stiffness and improve the convergence of results.

To represent the crush triggers machined on the ends of the channel (Figure 3.5), the first 2.5 mm to 3 mm of the hat channel were partitioned and meshed separately. These elements (green in Figure 4.2) were then assigned a shell thickness half of the thickness of the rest of the channel (0.17 mm per ply). Wade and Feraboli [39] suggest that the thickness chosen for the trigger acts as a softening of strength for the crash front similar to the effect of the SOFT parameters on the subsequent rows of elements in the channel. The peak force can be increased or reduced proportionally with increases and decreases in the thickness defined for the trigger.

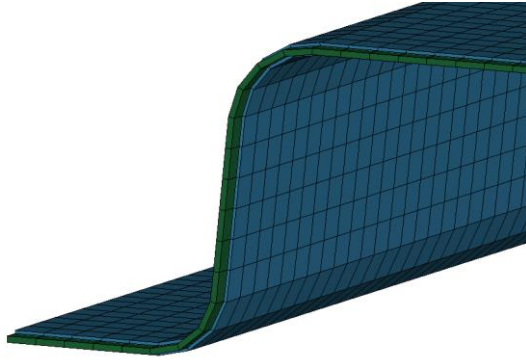


Figure 4.2: Finite element representation of the trigger (45° chamfer) at the crushing end of the hat channel.

Three coordinate systems exist for shells of anisotropic materials: global, element, and material. Orthotropic material models are defined with respect to the local material coordinate system [99] [109]. There are a few ways to set up the material axes for composite materials with shell elements in LS-DYNA: the AOPT options on the material cards, the BETA angle for each integration point, or via the element definition itself [99], [109]. For this study, the material card was set with AOPT = 0 to define the material as locally orthotropic with material axes determined by the element nodes. The elements were then manually aligned using the element editing tool in LS-PrePost. The direction of the elements was aligned with respect to a vector along the global X-axis (Figure 4.3). The change in material direction per integration point is visualized in Figure 4.4. Elements whose directions were changed were recreated as SHELL_BETA elements, such that the final channel part was a combination of SHELL_BETA and SHELL elements.

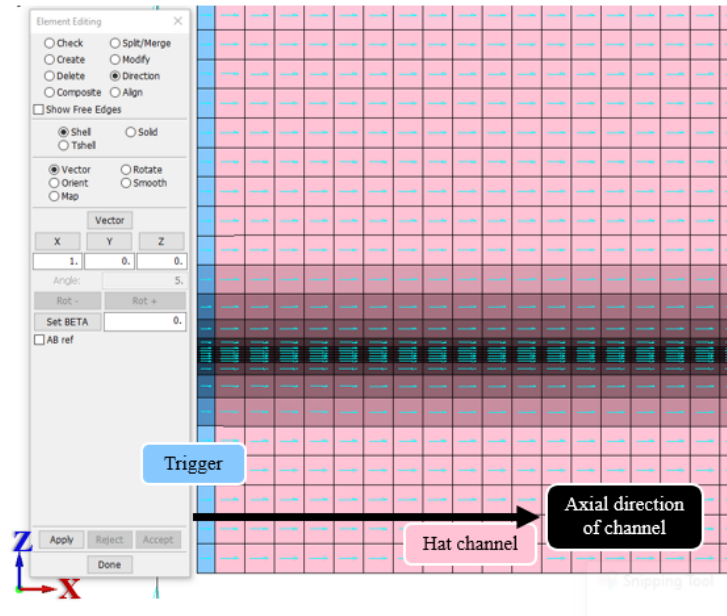


Figure 4.3: Quadrilateral elements of hat channel aligned in the axial (global X) direction using the element editing tool in LS-PrePost.

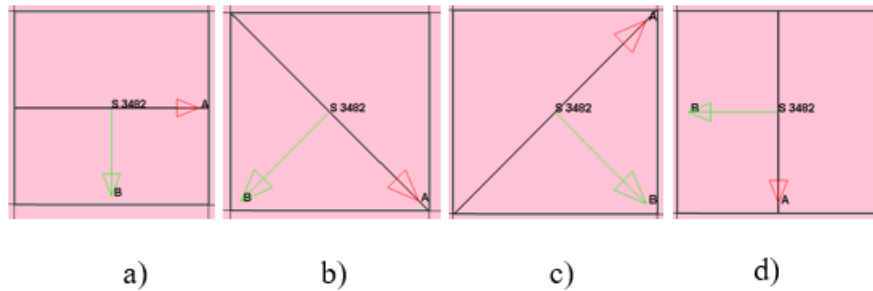


Figure 4.4: Material directions of shell element 3482 of the hat channel part for integration points 1 to 4: a) 0° , b) $+45^\circ$, c) -45° , d) 90° .

Three element mesh structures were considered for the hat channel: structured quadrilateral, unstructured triangle, and angled structured quadrilateral (Figure 4.5). It was suggested by Dharmaraj [37] that a mesh consisting of angled structured quadrilaterals improved the simulated force-displacement results for the axial loading of a channel by reducing or eliminating the numerically introduced oscillations typically. When employing a quadrilateral mesh, the stable crushing force regularly drops to zero, representing the deletion of an element/row of elements and the consequential

gap between the impacting sled and the next row of elements. For a constant velocity rigid wall, this results in a saw-tooth shape corresponding to element deletion and subsequent geometry reloading as opposed to the similarly appearing serrated saw-tooth behaviour present in the progressive failure of a brittle material [147]. Minimizing these oscillations to interpret the output usefully often requires filtering the data or damping [147] (Appendix D).

As a result, the baseline model was initially developed with an angled structured quadrilateral mesh applied. The non-uniform mesh structure facilitated a numerical crush progression such that there was always at least one element or group of elements in contact with the impacting sled, which minimized oscillations. However, this mesh structure was tedious to create and arbitrary in design. An unstructured triangular mesh was also tried and straightforward to implement. While it eliminated numerically introduced oscillations in the force-displacement response, triangular meshes are typically discouraged for crash analysis because the elements are too stiff [148]. It was recommended that they be avoided wherever possible except when applied to rigid bodies [148]. Thus, structured quadrilateral meshes were ultimately implemented in the model for all part representations, and the force-displacement results were filtered. Additional details on the influence of the mesh structures on the predicted force-displacement responses are provided in Appendix D. Lastly, based on previous work (i.e., Dharmaraj [37]) and mesh convergence studies (Appendix D), an element size of 5 mm for the structured quadrilateral elements was selected.

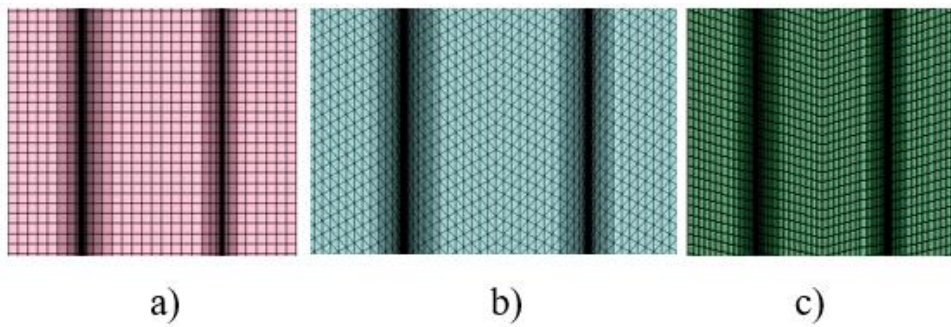


Figure 4.5: Elements generated and considered for the representation of the hat channel: a) structured quadrilateral, b) unstructured triangle, and c) angled structured quadrilateral.

To represent the impacting sled, fully integrated shell elements (Type 16) were generated at an element size of 20 mm $\times$ 20 mm with a thickness of 0.5 mm (Figure 4.1). Mass elements were added to each node such that the sum of all mass points was 858 kg (a total of 441 0.0018141-ton

mass elements). Refined meshes of rigid bodies were encouraged for more elastic mass distribution properties and distribution of contact forces [149], [150]. A material card was assigned to the shell part and is further discussed in Section 4.1.4.

4.1.2 Boundary & Loading Conditions

The impacting sled was constrained to only translate in the axial direction of the channel. For the dynamic conditions, an initial velocity of $7500 \frac{mm}{s}$ was assigned to every node of the sled (Figure 4.1). To replicate the constraints applied by the fixtures to secure the end of the channel to the vertical wall, the bottom 50 millimetres of the channel was constrained in all six degrees of freedom using single-point constraints (Figure 4.6). The influence of including the fixtures as rigid bodies as well as the contact types was explored in preliminary simulations and notable differences were not observed.

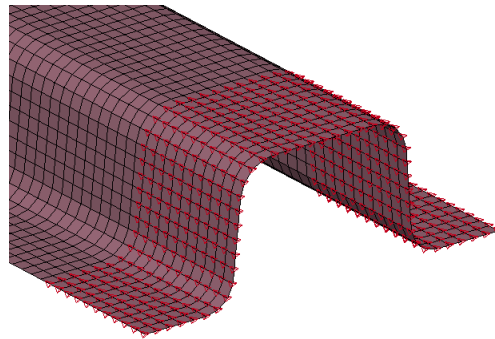


Figure 4.6: Single point constraints (all 6 DOFs) applied to the bottom 50 millimetres of the hat channel.

The penalty-based CONTACT_AUTOMATIC_SINGLE_SURFACE card was added to the simulation model. Single surface contacts are the most widely used type of contact for crashworthiness applications in LS-DYNA [150]. All parts in the model were considered as part of the slave surface, which allowed for consideration of contact between parts as well as the self-contact of parts. An automatic contact was used, as recommended [150], because the deformations were expected to be large and it could not be predetermined where and how contact would occur. The coefficients for dynamic and static friction were set to 0.2. To avoid initial penetrations, the parts were distanced at least half the thickness of the impacting sled elements [151]. The resultant forces were not directly retrievable in the RCFORC file with this contact card; thus, a

CONTACT_FORCE_TRANSDUCER_PENALTY card was also defined to retrieve the contact forces [150].

The termination time of the simulation model was set to 35 milliseconds, which corresponded to a sled displacement greater than 190 mm.

4.1.3 Other Simulation Model Additions

4.1.3.1 Numerical Consistency Between Runs

To effectively compare results and evaluate the influence of changing parameters in the model, it was necessary to confirm that numerical results would not change run-to-run for consistent parameters. To minimize concerns with precision, numerical consistency was enforced through the CONTROL_PARALLEL card [149], [104]. This forced the order of summations to occur in a precise order so that there was reduced randomness in the round-off error [105]. Setting a CONST=1 and NCPU to the negative of the number of CPUs being used provided identical results in terms of nodal displacements and element stresses (NODOUT and ELEOUT files) for the same file, as opposed to other ASCII values which may have summations operating in parallel (for efficiency) and thus cannot have the order of summing operation enforced [105]. It was confirmed that when re-running the same simulation model, the nodal displacement and element stresses did not change between runs, though the force-displacement results varied slightly.

4.1.3.2 Outputs

Shell integration points (MAXINT) corresponding to the number of plies being considered (i.e. eight) were added to the written binary database (DATABSE_EXTENT_BINARY) along with integration point history variables (NEIPS) corresponding to the history variable table outlined in Appendix A [99]). The NEIPS history variables tracked the mode of failure in each integration point. Additionally, within the binary database card, the stress and strain outputs were set to be written with respect to the material axes coordinate system by changing the CMPFLG from the default value to 1 [99]. This allowed for investigation of the predicted fibre and matrix stresses and strains in the ply directions [109].

For data outputs, element output data (ELOUT), global statistics (GLSTAT), material energies (MATSUM), nodal force groups (NODFOR), nodal displacement/velocity/acceleration data (NODOUT), rigid body data (RBDOUT), resultant interface forces (RCFORC), and SPC reaction

forces (SPCFORC), were outputted at 0.0001-second intervals. Similarly, the binary D3Plot was outputted at 0.0001-second intervals.

4.1.3.3 Control Cards

Several control cards were added to the simulation model to improve the stability of results based on recommendations from literature:

- Within the CONTROL_ACCURACY card, invariance node numbering was implemented for shell elements (INN = 2) to eliminate sensitivity to the order of nodes in the element connectivity [99], [149]. This was expected to minimize the sensitivity to in-plane shearing and hourglass deformation of orthotropic shells [109].
- CONTROL_BULK_VISCOSITY was added with the default bulk viscosity type being set to the standard for type 2, 10, and 16 shell elements (TYPE = -1) [149]. This option was suggested to aid in reducing the noise of the response [109].
- CONTROL_ENERGY was added with default values to initiate the computation of relevant energy values [149]. This allowed for review of the *glstat* and *matsum* energies, the hourglass energy and the contact energy. It is recommended that the energy ratios remain close to 1.0, the hourglass energy remain less than 10% of the peak internal energy, and the contact energy be positive [149]. These results were confirmed for the simulation model.
- CONTROL_PARALLEL was added to enforce numerical consistency between runs (NCPU = negative number of CPUs used, PARA = 2) [149], [104].
- Within the CONTROL_SHELL card, laminated shell theory was applied (LAMSH = 1). Additionally, shell thinning was not invoked (ISTUP = 0) to promote numerical stability since composites do not stretch significantly before breaking [109].
- When triangular shells were used, CONTROL_SHELL was set to ESORT = 1 [149].
- CONTROL_TERMINATION was set for 35 milliseconds.
- CONTROL_TIMESTEP was added with all defaults maintained.

4.1.4 Material Models

The part representing the sled was assigned rigid material properties (MAT_020) based on steel as outlined below (Table 4.1).

Table 4.1: Properties of steel used in MAT_20 to represent the impacting sled.

Material Property	Value
Density	$7890 \frac{kg}{m^3}$
Poisson's Ratio	0.29
Young's Modulus	207 GPa

The parts representing the channel and trigger were assigned material model MAT_054 (Enhanced_Composite_Damage). It has been well established that MAT_054 possesses many non-physical parameters that must be calibrated through trial-and-error [37], [39], [112]. The physical mechanical properties provided to MAT_054 were taken from Suratkar [140], while the initial non-physical parameters were selected based on recommendations from literature and LSTC documentation [32], [105]. The initial values are summarized in Table 4.2 and were first applied to the hat channel and trigger parts created with an angled quadrilateral mesh and then repeated with the structured quadrilateral mesh for practicality (Chapter 6).

Table 4.2: Initial parameters used for the calibration of material model, MAT_054, for the development of a simulation model capturing the baseline condition.

Parameter	Value	Justification for Value	Parameter	Value	Justification for Value
RO	1.46×10^{-9}	Experimentally derived material properties, Table 3.1 [140]	DFAILT	-	Overridden by EFS.
E _A	121000		EFS	0.34	Arbitrary
E _B	8000		TFail	2.5E-07	One magnitude smaller than the timestep of the smallest element.
PRBA	0.0215		ALPH	0.1	Recommended between 0 and 0.5 [105]
G _{AB}	3550		SOFT	0.50	Must be calibrated by trial and error [32]
G _{BC}	3550		FBRT	0.5	Recommended
G _{CA}	3550		YCFAC	1.2	Default
XC	1001		BETA	0.5	Recommended
X _T	1637		PEL	100	Default
Y _C	145		SLIMT1	1.0	Default
Y _T	60		SLIMT2	1.0	Default

SC	90	Overridden by EFS.	SLIMC1	1.0	Default
DFAILM	-		SLIMC2	1.0	Default
DFAILC	-		SLIMS	1.0	Default
DFAILS	-				

To generate a stable force-displacement response, additional adjustments via trial-and-error were required to the initial values outlined in Table 4.2. A parametric study (Table 4.3) was performed to identify the best combination of non-physical parameters that would replicate the average experimental force-displacement response. The final non-physical parameters for both the angled quadrilateral mesh and the structured quadrilateral mesh are discussed in Chapter 6.

Table 4.3: Values considered for the parametric studies of the “non-physical” parameters of MAT_054.

Parameter	Initial Value	Variation Considered
DFAILM	-	0.1,0.5,0.9
DFAILC	-	-0.1, -0.5, -0.9
DFAILS	-	0.1, 0.5, 0.9
DFAILT	-	0.1, 0.5, 0.9
EFS	0.3	0.1, 0.5, 0.9
TFAIL	2.5E-07	N/A
ALPH	0.1	0.5, 0.9
SOFT	0.50	0.1, 0.9
FBRT	0.5	0.1, 0.9
YCFAC	2.0	0.5, 1.0, 3.0, 6.0
BETA	0.5	0.1, 0.9
PEL	100	10, 50, 90
SLIMT1	1.0	0.1, 0.5, 0.9
SLIMT2	1.0	0.1, 0.5, 0.9
SLIMC1	1.0	0.1, 0.5, 0.9
SLIMC2	1.0	0.1, 0.5, 0.9
SLIMS	1.0	0.1, 0.5, 0.9

4.2 Development of Additional Single Channel Simulation Models

Additional simulation models were developed to predict the force-displacement response of open channels with different stacking sequences, geometries, loading rates, and loading conditions as follows:

- Dynamic axial loading of $[\pm 45/0_2]_s$ hat channel.
- Dynamic axial loading of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ corrugated and tapered channels.

- Quasi-static axial loading of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels.
- Dynamic three-point bending of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels.
- Quasi-static three-point bending of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels.

For each condition, the simulation model developed for the baseline condition (Section 4.1) was modified as needed, while the calibrated material model parameters (Section 6.1.3.10) were maintained. The details of the additional simulation models are provided in the following section. Changes in stacking sequence were accomplished by modifying the angle defined for the integration points within the PART_COMPOSITE card.

4.2.1 Dynamic Axial Loading of Tapered and Corrugated Channels

Besides replacing the geometry in the simulation model for the baseline condition with the corrugated and tapered channel geometries, the material models, loading conditions, contacts, and control cards already discussed were maintained in these additional simulation models. For both geometries, the element direction remained aligned with the global X-axis of the simulation model.

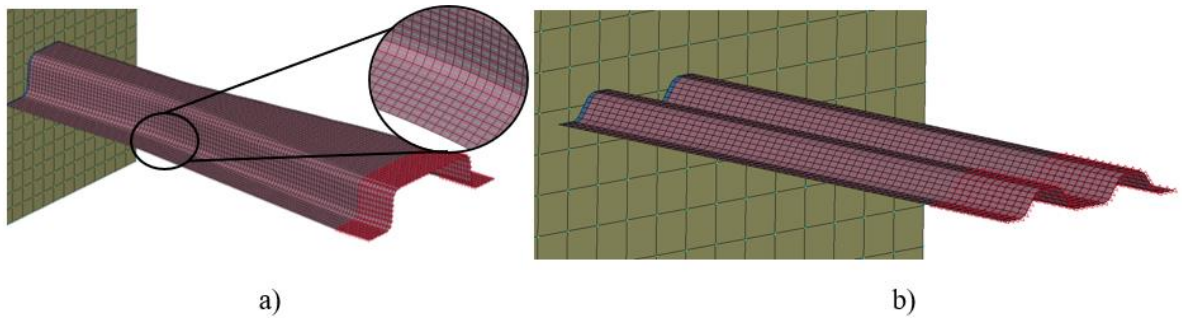


Figure 4.7: Finite element representation of the a) tapered channel, and b) corrugated channel.

4.2.2 Quasi-Static Axial Loading of the Hat and Corrugated Channels

The simulation models for the quasi-static axial loading of the hat and corrugated channels were developed by modifying the boundary and loading conditions defined for the impacting sled in the simulation model for the baseline condition. Additionally, the geometry and/or stacking sequence of the channels was updated as required. The mass of the impactor summed to 100 kg across all nodes in order to represent the plate used in the hydraulic press for the experiments [25]. The impactor was prescribed a constant displacement rate of $90 \frac{mm}{s}$, which was scaled from the experimental rate of

1mm/s to reduce computational time. It was verified in the quasi-static simulations that the kinetic energy remained <5% of the potential energy. The termination time was set to 1 second. Lastly, the length of the hat channel was reduced to 305 mm, which was consistent with the experimentally tested channels [25]. Quasi-static axial loading simulation models were only developed for the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels because corresponding experimental data was only available for these cases.

4.2.3 Dynamic & Quasi-Static Three-Point Bending of Hat and Corrugated Channels

For the three-point bending simulation models, the hat and corrugated channels remained modelled as described in Sections 4.1 and 4.2.1, respectively, except for the removal of the trigger elements and extension of the channel lengths to 600 mm. These changes were to align with the experimentally tested components. The impactors, fixtures, and boundary/loading conditions were also altered to replicate the experimental setup [25]. A half cylinder meshed of solid brick elements was used to represent the impact roller used experimentally [25] (Figure 4.8). Additionally, the default constant stress solid element (ELFORM = 1) was assigned to the impactor with the same material card and properties outlined in Table 4.1. The fixtures were assigned the same material properties and element type as well. The dimensions of the impactor and fixtures were created to replicate the experimental conditions (Figure 4.8) [25]. For the dynamic experiments, the resulting mass of the impactor and sled was 877 kg. Thus, mass elements amounting to 877 kg were added to the impactor for the dynamic simulation models. For the quasi-static simulation models, mass elements amounting to 50 kg were added to the cylindrical impactor.

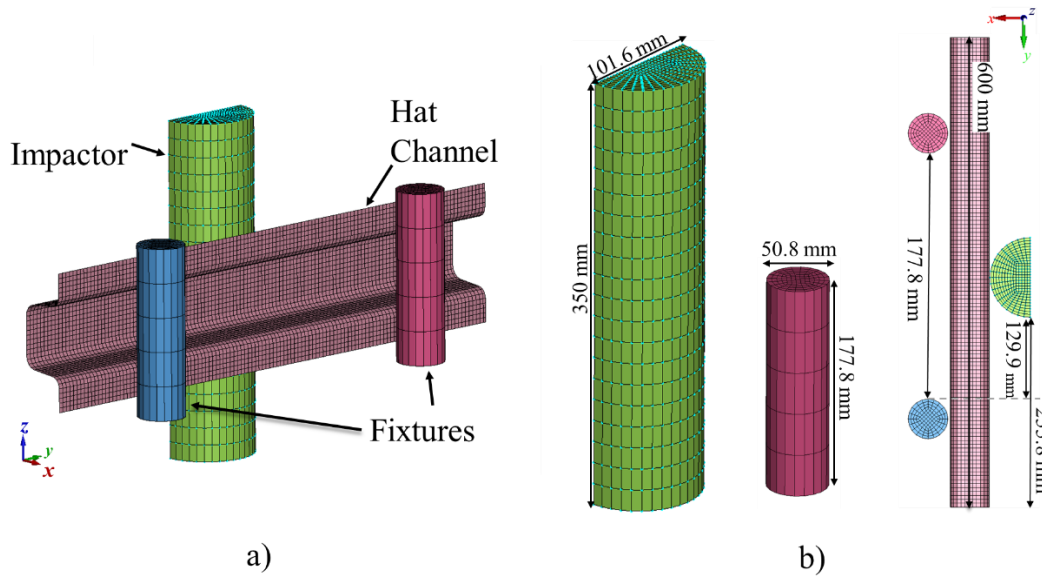


Figure 4.8: (a) Finite element representation of the parts created for the simulation models of the three-point bending of hat (and corrugated) channels. (b) Dimensions of parts.

For the dynamic model, the impactor was assigned an initial velocity of $7000 \frac{\text{mm}}{\text{s}}$ and constrained to translate only in the x -direction. For the quasi-static model, impact was scaled from the experimental loading rate of $1 \frac{\text{mm}}{\text{s}}$ in the x -direction to $90 \frac{\text{mm}}{\text{s}}$. The fixtures were constrained in all degrees of displacement, as well as x and y rotations (free to rotate in z -direction, as per axes in Figure 4.8). As with the simulation model for the baseline condition (Section 4.1), an AUTOMATIC_SINGLE_SURFACE contact was applied between all parts. However, the coefficient of friction was set to zero to replicate the Teflon sheets placed between the channels, supports and impactor during the experiments [25]. Once again, the force-transducer penalty card was applied to measure the reaction force of the impactor and was compared to the reaction forces of the fixtures to verify that they were notably the same. The same control cards applied to the simulation model for the baseline condition were applied to the three-point bending simulations as well. The termination time was set to 30 milliseconds for the dynamic model and 1 second for the quasi-static model.

4.3 Development of Double-Hat Channel Simulation Models

Additional finite element models were developed to simulate the quasi-static and dynamic axial loading of the double-hat channels. In this set of models, the hat channel part was duplicated and

two parts representing the layer of adhesive between each pair of flanges were added to the models. Additionally, a material model was added to capture the behaviour of the structural adhesive. Lastly, an additional contact card was defined to represent the bond between the layer of adhesives and each pair of flanges.

4.3.1 Additional Geometries & Meshes

Figure 4.9 displays the parts created to simulate the double-hat channel experiments described in Section 3.1. Two hat channels with structured quadrilateral meshes were created with the same material model parameters and part geometries selected for the simulation model of the baseline condition in Section 4.1. Similarly, the sled impactor remained modelled as was described in Section 4.1 but with structured quadrilateral elements of 7.8 mm by 12.6 mm.

The two adhesive parts were meshed using solid elements, namely 8-node cohesive elements with four integration points (ELFORM = 20) (Figure 4.9). Cohesive elements were selected because, as mentioned, they efficiently capture fracture at bonded joints without a significant increase in computational expense [152], [153], [154]. As with the hat channel meshes, a convergence study was completed to determine the size of the solid elements (Appendix E). It was concluded that 1 mm by 1 mm by 0.65 mm solid elements were sufficient to replicate the experimental performance of the adhesive.

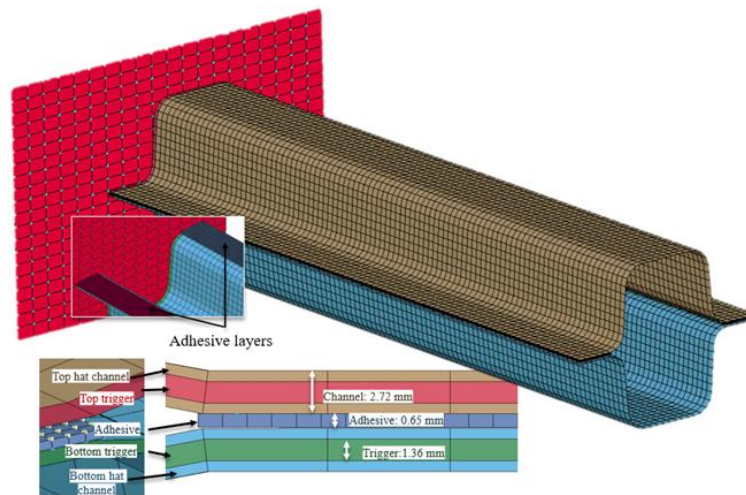


Figure 4.9: Parts created to simulate the dynamic and quasi-static axial loading of the double-hat channels.

4.3.2 Change in Boundary Conditions

The boundary conditions were maintained the same as the simulation model for the baseline condition, with the exception of additional contacts between the adhesive parts, hat channels, and triggers. The Tied_Shell_Edge_To_Surface_Constrained_Offset card was used to define connections between the adhesive and each pair of flanges/triggers while accounting for the shell thickness. Eight node sets were created to form the connections, and these were connected to the appropriate part of the hat channel and/or trigger. Figure 4.10 illustrates the four node sets/contacts created for the left-hand flanges, which were then duplicated for the right-hand side for a total of eight node sets.

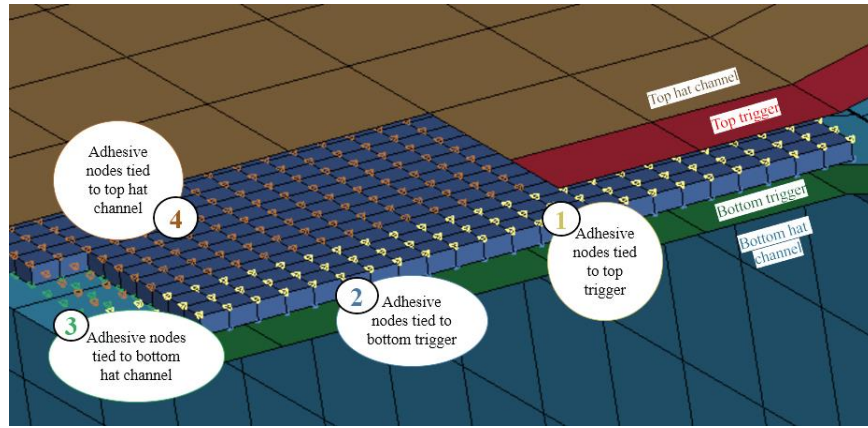


Figure 4.10: Node sets created to define connections between the hat channel/trigger parts and the adhesive part for one pair of flanges.

4.3.3 Material Model for Structure Adhesive

Both bilinear and trilinear traction-separation cohesive models were considered for the representation of the adhesive. Preliminary simulation models of DCB and ENF tests were created to verify the accuracy of the material models and compared with available experimental data for Mode I [120] and Mode II [45] loading of a UD-NCF composite bonded with 3M 07333 (Appendix E). Ultimately, MAT_240 (trilinear traction-separation law) was found to provide the most agreeable results using parameters determined by Watson [142] for the same adhesive. The selected parameters for MAT240 are summarized in Table 4.4.

Table 4.4: MAT_240 material model parameters used to represent the behaviour of structural adhesive, 3M 07333.

Parameter	Value
Density	$1200 \frac{kg}{m^3}$ [120]
Young's Modulus	1.259 GPa [142]
Shear Modulus	0.760 GPa [142]
G_{IC} (Mode I energy release rate)	2200 [142]
G_{IIC} (Mode II energy release rate)	1369 [142]
T_0 , Yield stress in Mode I	49 MPa [142]
S_0 , Yield stress in Mode II	26 MPa [142]
F_{G1} , Parameter to describe the tri-linear shape of the traction-separation law in Mode I.	0.36 [142]
F_{G2} , Parameter to describe the tri-linear shape of the traction-separation law in Mode II	0.96 [142]

Chapter 5

Experimental Characterization Results & Discussion

Microscopic images of samples extracted from manufactured hat and tapered hat channels are first presented in this chapter to assess their microstructures and identify potential process-induced defects which may have influenced the energy-absorbing capacity of the channels (Section 5.1). Next, the results of the axial loading experiments performed on the tapered hat channels (Section 5.2) and double hat channels (Sections 5.3 – 5.5) are presented and discussed. For each set of repeated tests, the experimental force-displacement response and total energy absorption are presented along with the average in black. Average peak force, average mean crush force, average specific energy absorption, and average crush force efficiency are presented as performance metrics for comparison between the different geometries and stacking sequences. Performance metrics calculated for experimental work completed by Harvey [24] on the dynamic and quasi-static axial loading of hat and corrugated channels are also presented for comparison to channels of similar stacking sequence or geometry.

5.1 Microstructure of Untested Hat and Tapered Hat Channels

Longitudinal samples were extracted from a manufactured $[0/\pm 45/90]_s$ hat channel (Figure 5.1) and a $[\pm 45/0_2]_s$ tapered hat channel (Figure 5.2) using a DX-350 N tile saw (Rubi), and subsequently polished and inspected using an opto-digital microscope. Microscopic images were obtained at the top and bottom corners of both channels, specifically near the taper for the tapered channel. Manufacturing-induced defects within these regions were expected to be most prominent due to stretching and shearing of the fabric layers during forming. Measurements were taken at several locations along the samples to investigate variations in tow width and thickness as indications of misalignment.

The $[0/\pm 45/90]_s$ hat channel was 36% thinner at section B-B compared to section A-A (Figure 5.1), which is due to additional compaction of the fabric layers at the bottom corner of the channel during fabrication. The channel was moulded in a tool in which section B-B was at the bottom, which supports the possibility that more pressure was applied to this corner, leading to a greater degree of localized compaction of the fabric layers. The local fibre volume fraction was approximated using the following equation for reinforcement content [155]:

$$V_r = \frac{A_r N}{\rho_c h} \quad (34)$$

where A_r , is the areal weight of the fabric, N is the number of plies, ρ_c , is the density of the sample, and h is the thickness of the sample. For the material used in this study, the areal weight of the carbon fibre fabric (excluding the glass and polyester stitching) was $309 \frac{g}{m^2}$ while the total fabric areal weight was $333 \frac{g}{m^2}$ (Appendix B). The fibre density was $1.81 \frac{g}{cm^3}$ (Appendix B), with eight plies in each sample and thicknesses as indicated in the microstructure images. Thus, the local fibre volume fraction was calculated to be 54% at section B-B and 40% at section A-A, which are respectively higher and lower than the fibre volume fraction reported for samples taken from flat panels of the same material system (53%) [140].

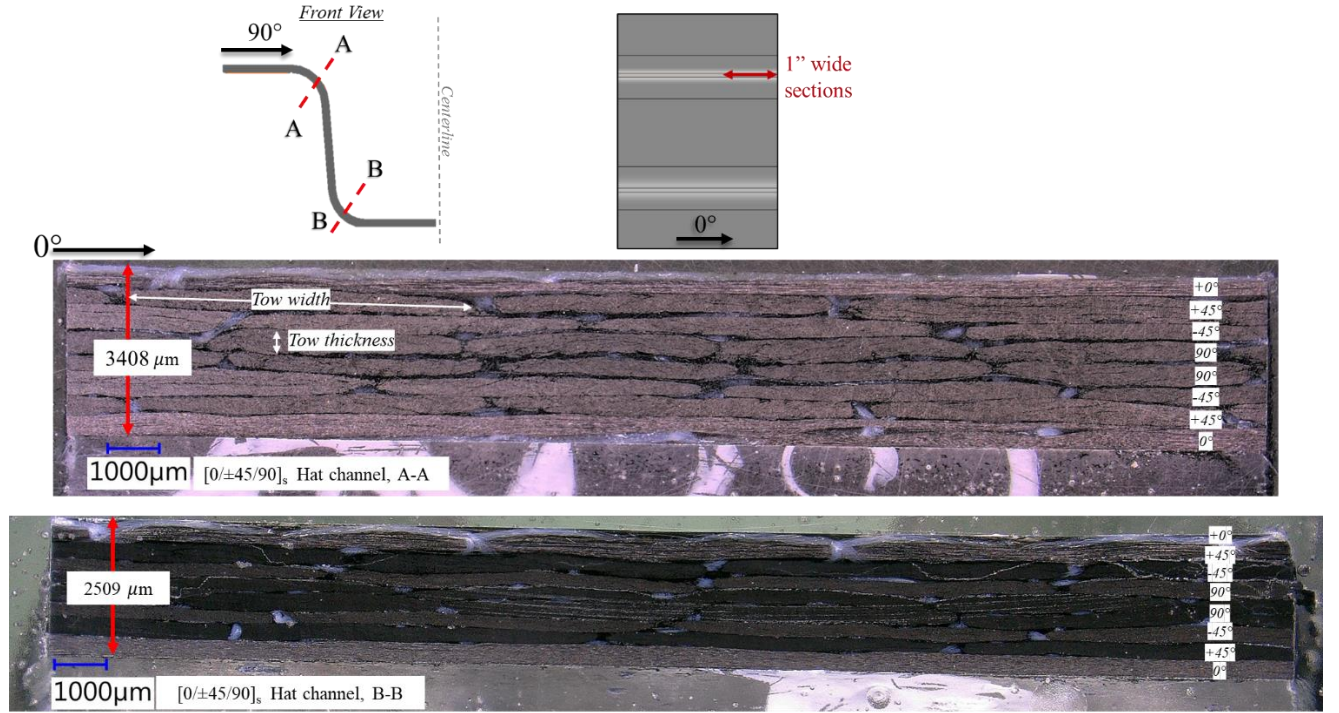


Figure 5.1: Microstructure of a $[0/\pm 45/90]_s$ hat channel at sections A-A and B-B.

Measurements of tow width were limited to regions in the microscopic images where the entire tow cross section was visible. The average tow width and variation in the thickness of individual plies were similar for both sections. Based on these measurements (Table 5.1 and Table 5.2), the average tow width in section B-B was $7344 \mu m \pm 149 \mu m$ for the $\pm 45^\circ$ plies and $5041 \mu m$ for

the 90° plies. Assuming that 90° plies are aligned appropriately in the layup, the average tow width of the $\pm 45^\circ$ plies was expected to be approximately 7129 μm based on sectioning a 90° tow at a 45° angle. This suggests that there was minimal misalignment of the $\pm 45^\circ$ tows within this section as the average is close to the expected value. In section A-A, the average tow width for the $\pm 45^\circ$ plies was $7054 \mu\text{m} \pm 336 \mu\text{m}$ which was slightly less than in section B-B. The average tow width for the 90° plies was $5091 \mu\text{m} \pm 150 \mu\text{m}$, which is similar to section B-B. The $\pm 45^\circ$ plies appeared to be only slightly misaligned as they were less than the expected width of 7200 μm . Thus, the plies appeared to be consistent in both tow width and alignment for both sections. Moreover, the average tow thickness in section B-B was $302 \mu\text{m} \pm 47 \mu\text{m}$ for the $\pm 45^\circ$ plies and $319 \mu\text{m} \pm 66 \mu\text{m}$ for the 90° plies. In section A-A, the average tow thickness for the $\pm 45^\circ$ plies was $408 \mu\text{m} \pm 70 \mu\text{m}$ while the average tow thickness for the 90° plies was $395 \mu\text{m} \pm 58 \mu\text{m}$. As expected based on the overall section thickness, the average tow thickness for the 90° and $\pm 45^\circ$ plies at section B-B was less than at section A-A due to increased fabric compaction at this location.

Table 5.1: Average tow width and thickness of the plies in Section A-A of a $[0/\pm 45/90]_s$ hat channel.

Ply Angle	Average Tow Thickness	Average Tow Width
+45°	$456 \mu\text{m} \pm 59 \mu\text{m}$	$7190 \mu\text{m} \pm 236 \mu\text{m}$
-45°	$383 \mu\text{m} \pm 67 \mu\text{m}$	$6721 \mu\text{m} \pm 323 \mu\text{m}$
90°	$434 \mu\text{m} \pm 55 \mu\text{m}$	$5157 \mu\text{m} \pm 73 \mu\text{m}$
90°	$363 \mu\text{m} \pm 37 \mu\text{m}$	$5024 \mu\text{m} \pm 176 \mu\text{m}$
-45°	$367 \mu\text{m} \pm 58 \mu\text{m}$	$6898 \mu\text{m} \pm 82 \mu\text{m}$
+45°	$435 \mu\text{m} \pm 54 \mu\text{m}$	$7354 \mu\text{m} \pm 111 \mu\text{m}$

Table 5.2: Average tow width and thickness of the plies in Section B-B of a $[0/\pm 45/90]_s$ hat channel.

Ply (Angle)	Average Tow Thickness	Average Tow Width
+45°	$334 \mu\text{m} \pm 48 \mu\text{m}$	$7143 \mu\text{m} \pm 41 \mu\text{m}$
-45°	$272 \mu\text{m} \pm 23 \mu\text{m}$	$7449 \mu\text{m} \pm 41 \mu\text{m}$
90°	$310 \mu\text{m} \pm 66 \mu\text{m}$	5041 μm
90°	$331 \mu\text{m} \pm 65 \mu\text{m}$	-
-45°	$280 \mu\text{m} \pm 34 \mu\text{m}$	-
45°	$316 \mu\text{m} \pm 45 \mu\text{m}$	$7439 \mu\text{m} \pm 51 \mu\text{m}$

For the $[\pm 45/0_2]_s$ tapered hat channel samples (Figure 5.2), section B-B was also thinner than section A-A, albeit less significantly than the hat channel samples, which may be due to the different

stacking sequences. Using the same method outlined above (Equation 35), for which only the thickness, h , changes, the local fibre volume fraction was calculated to be 44% for section A-A and 45% for section B-B. Moreover, it can also be observed that there was significant waviness in the 0° plies at section B-B. Measurements across these plies revealed an average crimp angle of $12 \pm 2^\circ$. This out-of-plane crimping may be the result of shearing and local wrinkling of the fibre tows during the forming of the fabric over a region of the tool where there exists a double curvature (i.e. along both the radius and the taper outwards) [156].

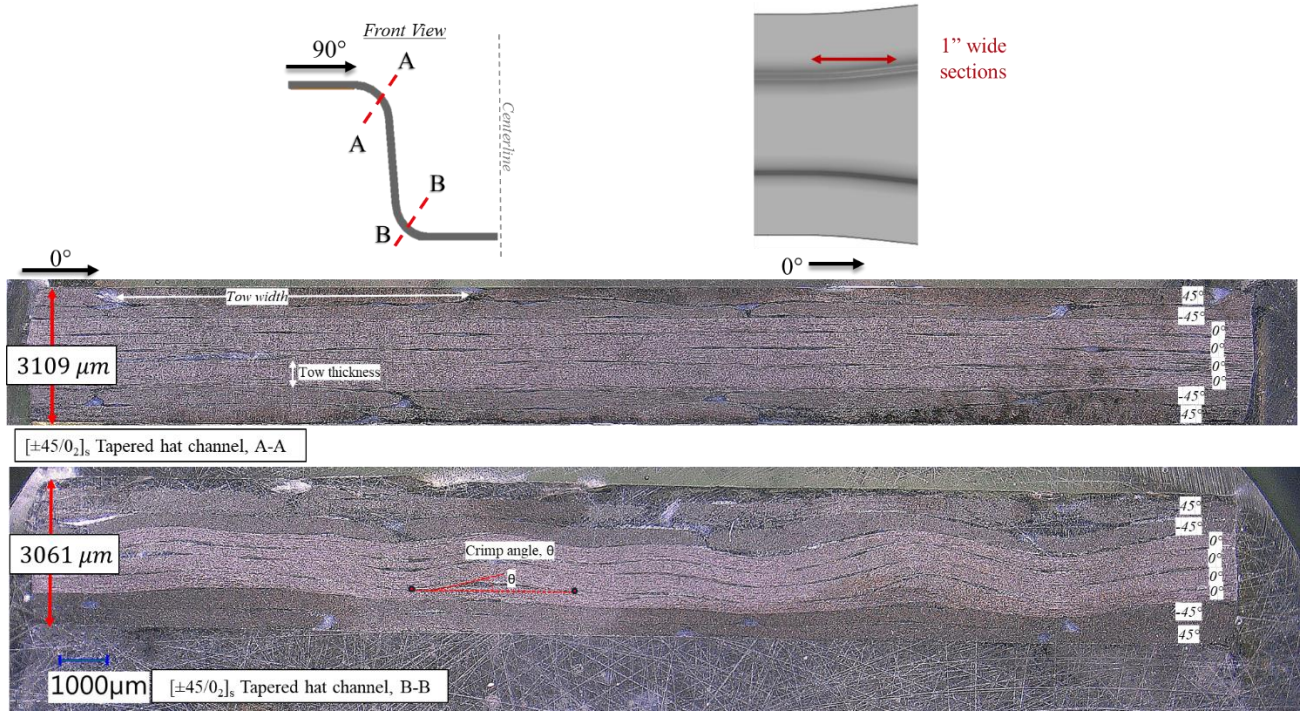


Figure 5.2: Microstructure of a $[\pm 45/0_2]_8$ tapered hat channel at sections A-A and B-B.

Based on the measurements that could be collected (Table 5.3 and Table 5.4), the average tow width in section B-B was $7270 \mu\text{m} \pm 68 \mu\text{m}$ for the $\pm 45^\circ$ plies. In section A-A, the average tow width for the $\pm 45^\circ$ plies was $6478 \mu\text{m} \pm 640 \mu\text{m}$. Since the cross-section was aligned along the 0° ply direction, tow widths for these plies could not be measured. It can be observed that the average tow width of the $\pm 45^\circ$ plies of section B-B was comparable to the average tow widths of $\pm 45^\circ$ plies measured for the hat channel, suggesting minimal misalignment in this section. However, there was a noticeable deviation ($\sim 11\%$) for the tow widths of $\pm 45^\circ$ plies measured for section A-A of the tapered hat channel, suggesting misalignment in this region. Also, the average tow thickness at

section A-A was $352 \mu\text{m} \pm 73 \mu\text{m}$ for the $\pm 45^\circ$ plies. At section B-B, the average tow thickness for the $\pm 45^\circ$ plies was $345 \mu\text{m} \pm 55 \mu\text{m}$. The thicknesses of the $\pm 45^\circ$ plies were similar across both sections.

Table 5.3: Average tow width and thickness of the plies in Section A-A of a $[\pm 45/0_2]_s$ tapered hat channel.

Ply Angle	Average Tow Thickness	Average Tow Width
+45	$364 \mu\text{m} \pm 50 \mu\text{m}$	$7265 \mu\text{m} \pm 143 \mu\text{m}$
-45°	$308 \mu\text{m} \pm 49 \mu\text{m}$	$6490 \mu\text{m} \pm 247 \mu\text{m}$
-45°	$339 \mu\text{m} \pm 36 \mu\text{m}$	$6211 \mu\text{m} \pm 108 \mu\text{m}$
45°	$406 \mu\text{m} \pm 45 \mu\text{m}$	$8102 \mu\text{m}$

Table 5.4: Average tow width and thickness of the plies in Section B-B of a $[\pm 45/0_2]_s$ tapered hat channel.

Ply Angle	Average Tow Thickness	Average Tow Width
+45°	$364 \mu\text{m} \pm 50 \mu\text{m}$	$7327 \mu\text{m} \pm 21 \mu\text{m}$
-45°	$308 \mu\text{m} \pm 49 \mu\text{m}$	-
-45°	$316 \mu\text{m} \pm 45 \mu\text{m}$	-
45°	$423 \mu\text{m} \pm 34 \mu\text{m}$	$7214 \mu\text{m} \pm 51 \mu\text{m}$

5.2 Dynamic Axial Loading of Tapered Hat Channels

The force-displacement results and performance metrics of the dynamic (7.5 m/s) axial loading of tapered hat channels with $[0/\pm 45/90]_s$, $[\pm 45/0_2]_s$, and $[0/90/\pm 45]_s$ stacking sequences are presented hereafter.

5.2.1 $[0/\pm 45/90]_s$ Tapered Hat Channels

For the $[0/\pm 45/90]_s$ tapered hat channels, the force-displacement response was consistent amongst the repeated tests with an average crush force of $38.6 \pm 0.6 \text{ kN}$, an average peak force of $92.1 \pm 1.3 \text{ kN}$, an average total energy absorption of $11.0 \pm 1.0 \text{ kJ}$, an average specific energy absorption of $44.5 \pm 0.2 \frac{\text{kJ}}{\text{kg}}$, and an average crush force efficiency of $41.91 \pm 1.0\%$ (Figure 5.3 and Figure 5.4). For the first trial, a drop in the force deviating from the other two channels occurred after approximately 225 mm because the channel failed via local buckling in one of the flanges (Appendix F). As a result, for this trial, the mean crush force was measured based on the response for 200 mm of crush distance instead of 300 mm. Representative images of the crush progression during testing

highlight the magnitude of fragmentation during crushing as well as the local bending of the channel downwards with increasing impactor displacement (Figure 5.3).

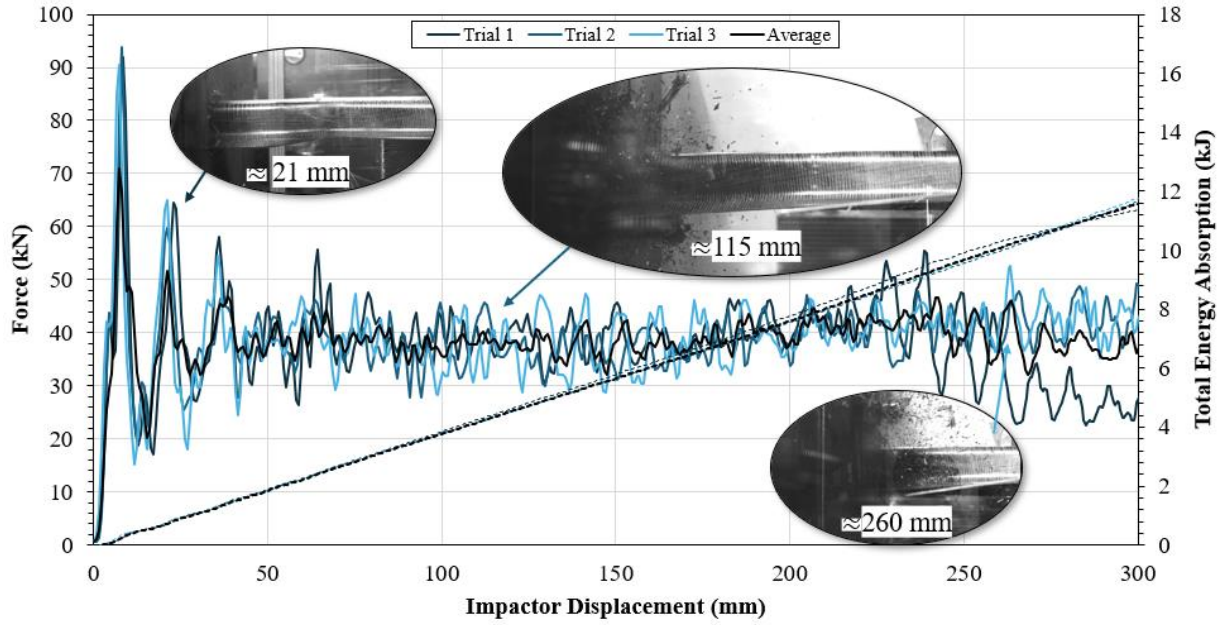


Figure 5.3: Force-displacement response of $[0/\pm 45/90]_s$ tapered hat channels under dynamic axial loading.

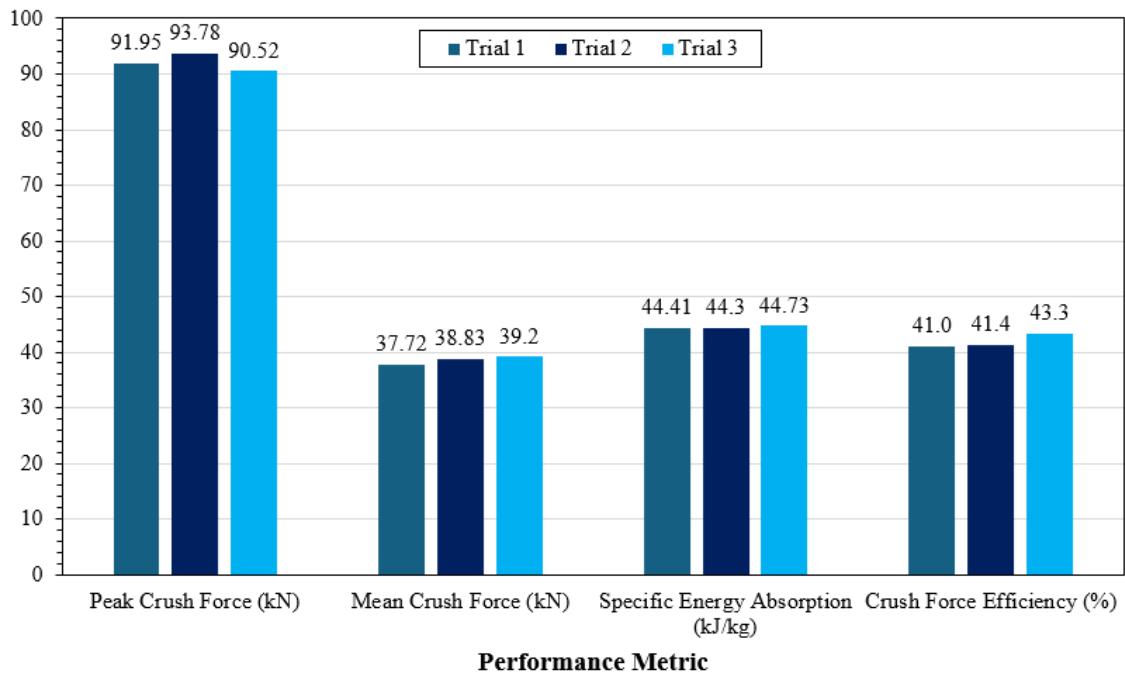


Figure 5.4: Performance metrics of $[0/\pm 45/90]_s$ tapered hat channels under dynamic axial loading.

The common failure mode observed in the $[0/\pm 45/90]_s$ tapered hat channels was fragmentation (Figure 5.5). Characteristics of this failure mode are short interlaminar cracks (i.e., localized delamination) and the fracture of fibre bundles via transverse shearing (Figure 2.13). As can be seen in the images of the channels post-testing, all three tests exhibited minimal or no fronds remaining with significant fractures of fibre bundles. It can be observed in the latter two images, that portions of the outer 0° plies remained intact as cracks propagated parallel to these fibre bundles (aligned with the loading direction).

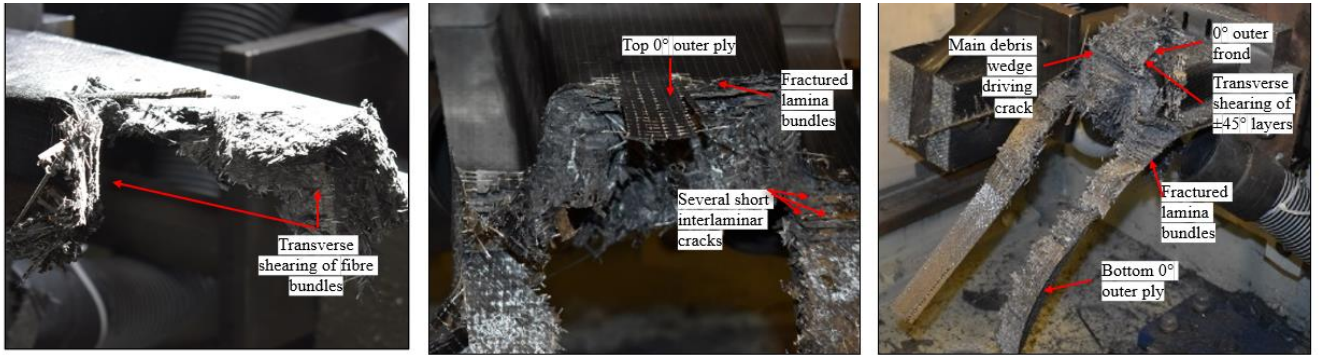


Figure 5.5: Images of the [0/±45/90]_s tapered hat channels after dynamic axial loading exhibiting the general failure mode, fragmentation.

5.2.2 [0/90/±45]_s Tapered Hat Channels

For the [0/90/±45]_s tapered hat channels, the average crush force was 37.7 ± 0.37 kN, the average peak force was 87.7 ± 10.96 kN, the average total energy absorption was 11.3 ± 0.1 kJ, the average specific energy absorption was $41.8 \pm 0.57 \frac{\text{kJ}}{\text{kg}}$ and the average crush force efficiency was $43.8 \pm 6.3\%$ (Figure 5.6 and Figure 5.7). Good consistency between repeated tests and progressive crushing were observed.

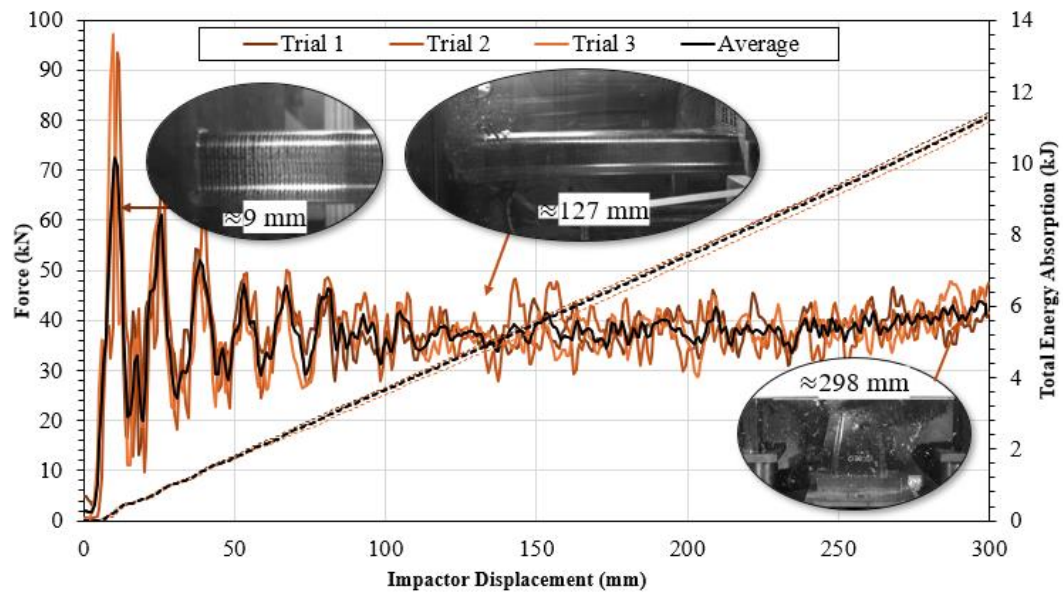


Figure 5.6: Force-displacement response of [0/90/±45]_s tapered hat channels under dynamic axial loading.

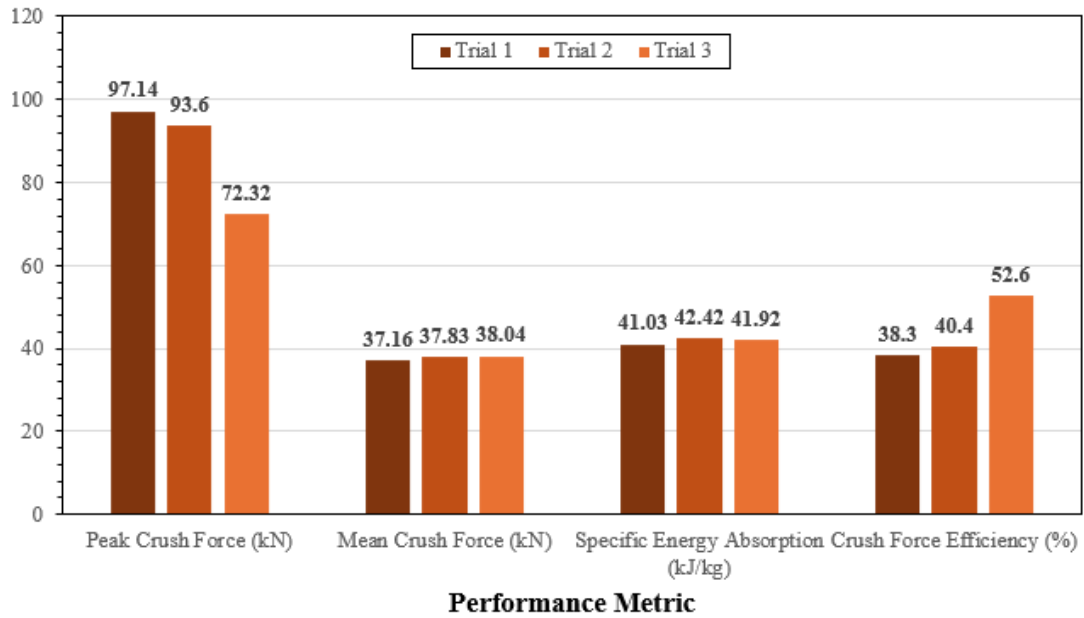


Figure 5.7: Performance metrics of $[0/90/\pm 45]_s$ tapered hat channels under dynamic axial loading.

Similar to the previous quasi-isotropic stacking sequence, the predominant failure mode for all channels was fragmentation. The crushed specimens exhibited significant short interlaminar cracks (Figure 5.8). In contrast to the $[0/\pm 45/90]_s$ channels, it was observed that the outer 0° plies fragmented less during loading, with frond formation focused on the flanges and transverse shearing of the fibre bundles predominant at the channel radii. This may be because the increased difference in angle between the 0° plies and the adjacent 90° plies led to higher interlaminar stresses that drove delamination cracks between these layers as opposed to crack propagation through the layers. Lastly, it was observed that while there were more fronds remaining, there was less bending of the fronds in comparison to the aforementioned stacking sequence, particularly near the crush front. This translated to a slightly reduced energy absorption than the previous stacking sequence.

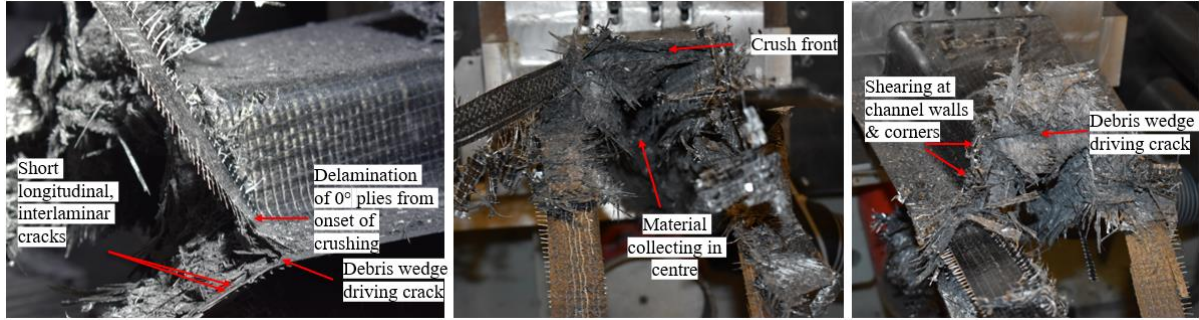


Figure 5.8: Images of the $[0/90/\pm 45]_s$ tapered hat channels after dynamic axial loading exhibiting the general failure mode, fragmentation.

5.2.3 $[\pm 45/0_2]_s$ Tapered Hat Channels

Lastly, for the $[\pm 45/0_2]_s$ tapered hat channels, the average crush force was 53.2 ± 0.7 kN, the average peak force was 115.1 ± 11.3 kN, the average total energy absorption was 16.5 ± 0.3 kJ, the average specific energy absorption was $58.9 \pm 0.7 \frac{\text{kJ}}{\text{kg}}$, and the average crush force efficiency was $71.0 \pm 3.4 \%$ (Figure 5.9 and Figure 5.10). The force-displacement responses demonstrated changes in mean crush force with increases in the cross-sectional area after an impactor displacement beyond 200 mm. In the transition region from the initial to tapering cross-section, there was a drop in load near 200 mm of displacement, which may have corresponded to the observed defects (Figure 5.2). Near the transition region, microscopic images of this stacking sequence revealed waviness of the 0° plies, which may have led to reduced efficiency in energy absorption and, thus, a lower mean crush force. As the cross-section increases in scale, so does the load-carrying capacity of the channel which causes an increase in the force beyond 220 mm of displacement.

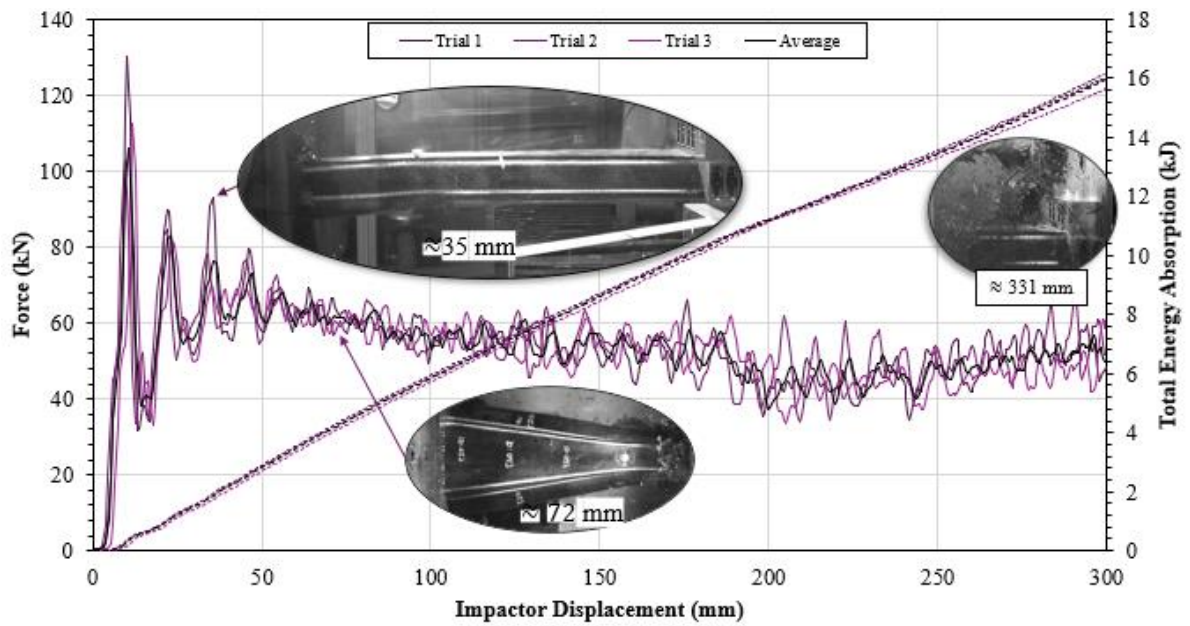


Figure 5.9: Force-displacement response of $[\pm 45/0_2]_s$ tapered hat channels under dynamic axial loading.

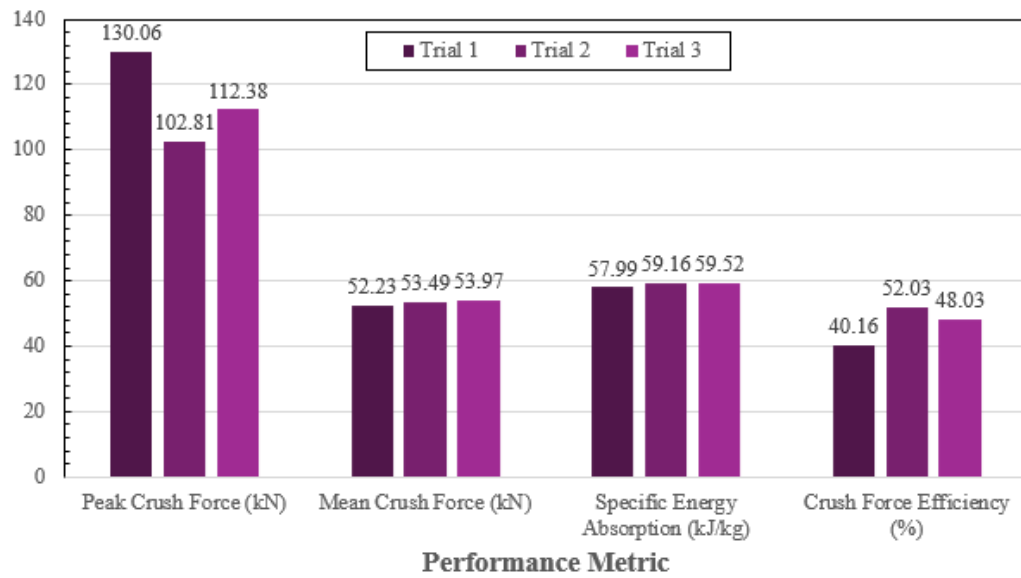


Figure 5.10: Performance Metrics of $[\pm 45/0_2]_s$ Tapered Hat Channels Under Dynamic Axial Loading

For the $[\pm 45/0_2]_s$ stacking sequence, the predominant failure mode was lamina bending/splaying and brittle fracture (Figure 5.11). Delamination cracks propagated between the 0° plies and the adjacent $\pm 45^\circ$ plies, where long sections of fibre bundles remained intact. Amongst the $\pm 45^\circ$ plies, a combination of short and long fibre bundles remained. Also characteristic was the fracture of bent fronds, observed amongst all three post-test specimens.

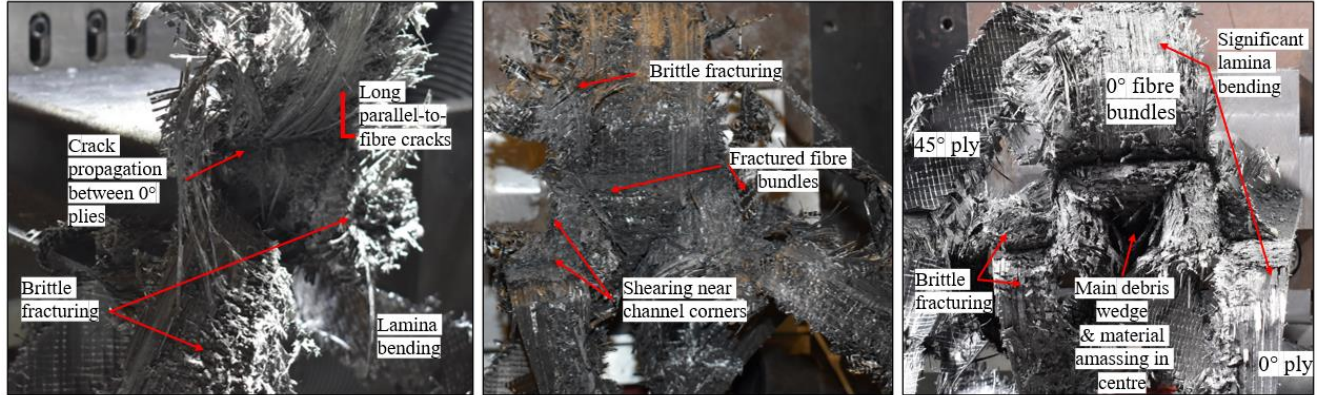


Figure 5.11: Images of the $[\pm 45/0_2]_s$ tapered hat channels after dynamic axial loading exhibiting the general failure mode, splaying/lamina bending.

5.2.4 Discussion of Tapered Hat Channel Results

The performance metrics of tapered hat channels for each stacking sequence were compared for an impactor displacement up to 300 millimetres (Figure 5.12). The SEA of the $[\pm 45/0_2]_s$ channel was greater than that of the $[0/\pm 45/90]_s$ and $[0/90/\pm 45]_s$ channels by 24.5% and 29.0%, respectively. Next, the peak and mean crush force values for the channels with the two quasi-isotropic stacking sequences differed slightly by 4.8% and 2.3%, respectively. This suggests that the order of the $\pm 45^\circ$ and 90° plies within the quasi-isotropic stacking sequences does not significantly influence the performance of the channel.

The difference in performance among the channels with different stacking sequences can be attributed to the difference in damage mechanisms observed. Fragmentation was predominant in the channels with quasi-isotropic stacking sequence (Figure 5.5 and Figure 5.8), while splaying was predominant in the other channels (Figure 5.11). This phenomenon aligned with Hull's conclusion that crush tubes with a larger proportion of hoop fibres tend to fail via fragmentation since the hoop fibres constrain against splaying, while splaying is predominant in tubes with more axially oriented

fibres [24]. The reduced fragmentation in the $[\pm 45/0_2]_s$ channels allowed for increased energy dissipation via different friction mechanisms, including between fronds of adjacent plies, and with the platen [14]. Additionally, delamination and the bending of lamina bundles resulted in energy absorption and additional crush area for frictional dissipation [14]. Lastly, the $[\pm 45/0_2]_s$ channels consisted of two more 0° plies than the other stacking sequences, which may have also contributed to increased energy absorption..

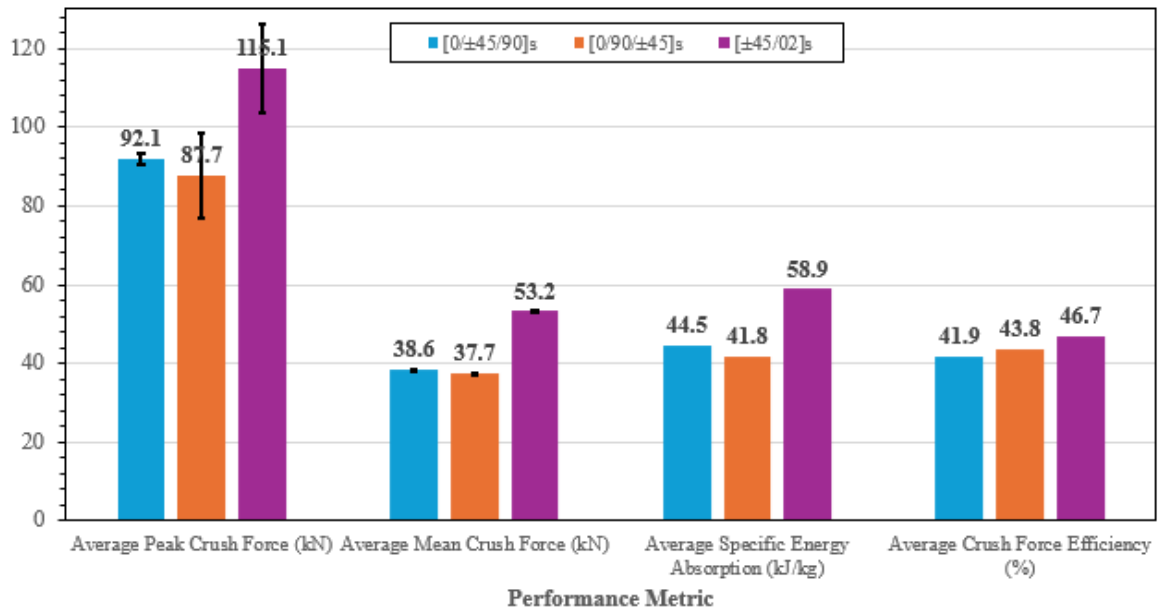


Figure 5.12: Comparison of tapered hat channel dynamic axial loading performance metrics. The scatter of the experimental data, notably for the latter two metrics, was minimal.

A comparison of the energy absorbed by the straight portion of the tapered hat channels (initial 200 mm) to the energy absorbed by the tapered portion (200 mm to 300 mm) revealed that the former is more effective in absorbing energy for the $[\pm 45/0_2]_s$ channels (Figure 5.13). For the channels with a quasi-isotropic stacking sequence, the energies absorbed by the straight and tapered portions were similar in magnitude. This suggested that there were inefficiencies introduced along the tapering portion, most pronounced for the $[\pm 45/0_2]_s$ channels.

For the channels with quasi-isotropic stacking sequences, the difference in SEA between the constant and tapered portions was limited to $<10\%$. In contrast, the differences in SEA between the constant and tapered portions of the $[\pm 45/0_2]_s$ channels were 20.8%, 16.7%, and 25.8%. The difference may be more pronounced for the $[\pm 45/0_2]_s$ channels because of the increased number of 0°

plies which were misaligned from the transition region into the taper and onwards (Figure 5.2). Misalignment or increased waviness in the 0° plies may have reduced the energy required to propagate cracks between the plies. As a result, splaying was still observed but less energy was absorbed. Since the inner plies of the quasi-isotropic stacking sequences were already not aligned with the axial direction, additional misalignment or waviness would not impact performance as significantly as the $[\pm 45/0_2]$ tapered hat channels. Nonetheless, even the tapering portions of the $[\pm 45/0_2]$ channels exhibited higher SEA values than the constant sections of the quasi-isotropic channels. Thus, the $[\pm 45/0_2]_s$ channels remained the most efficient energy absorbers under dynamic axial crushing.

The performance of the portion of the tapered hat channel with a constant cross-section was then compared to that of the previously tested hat and corrugated channels [25] (Figure 5.13). Firstly, it can be concluded that regardless of geometry, the $[\pm 45/0_2]_s$ stacking sequence was a more efficient energy absorber than the quasi-isotropic stacking sequences. Beyond this, if considered exclusively, the hat and corrugated channels suggest that geometry is of little influence on the performance of the channels when compared to the influence of the stacking sequence. However, the results of the tapered hat channel experiments provide insight that geometry is more influential than stacking sequence when it limits the predominant energy-absorbing mechanism.

Furthermore, the difference in the specific energy absorption of the first 200 mm of the $[\pm 45/0_2]_s$ tapered hat channel (62.4 kJ/kg) when compared to the hat channel (88.8 kJ/kg) highlights that there were also efficiency reducers within this initial section of the tapered channels. For the first 200 mm, the geometry of both channels was very similar (Figure 2.16 and Figure 3.1). The detractors are likely manufacturing defects which limited the quality of the part and resulted in the 25% reduction in SEA (Figure 5.2). Less significant differences were seen in the quasi-isotropic stacking sequences though they were likely to have the same distribution of defects. Thus, it can also be concluded that defects within channels that have more plies in the axial direction were more influential than defects in channels with a greater distribution of angled plies.

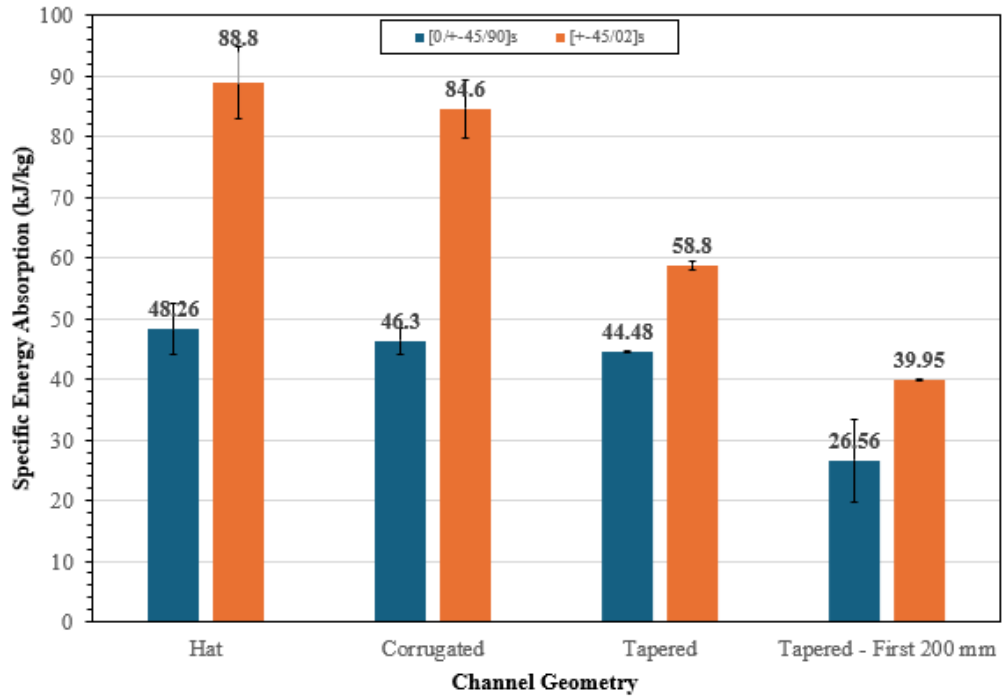


Figure 5.13: Comparison of the specific energy absorption of channels subject to dynamic axial loading per geometry and stacking sequence. Experimental results for the hat and corrugated channels taken from [25], [76].

5.3 Dynamic Axial Loading of Double-Hat Channels

The experimental results of the dynamic axial loading of $[0/\pm 45/90]_s$ and $[\pm 45/0]_2$ double-hat channels are summarized and discussed below.

5.3.1 $[0/\pm 45/90]_s$ Double-Hat Channels

For the $[0/\pm 45/90]_s$ double-hat channels, there was an average mean crush force of 76.8 ± 5.4 kN, an average peak force of 167.3 ± 2.5 kN, an average total energy absorption of $6.91 \text{ kJ} \pm 0.5 \text{ kJ}$, an average specific energy absorption of $44.3 \pm 2.4 \frac{\text{kJ}}{\text{kg}}$, and an average crush force efficiency of $45.9 \pm 2.6\%$ (Figure 5.14 and Figure 5.15). The force-displacement responses revealed good consistency between most trials and progressive crushing.

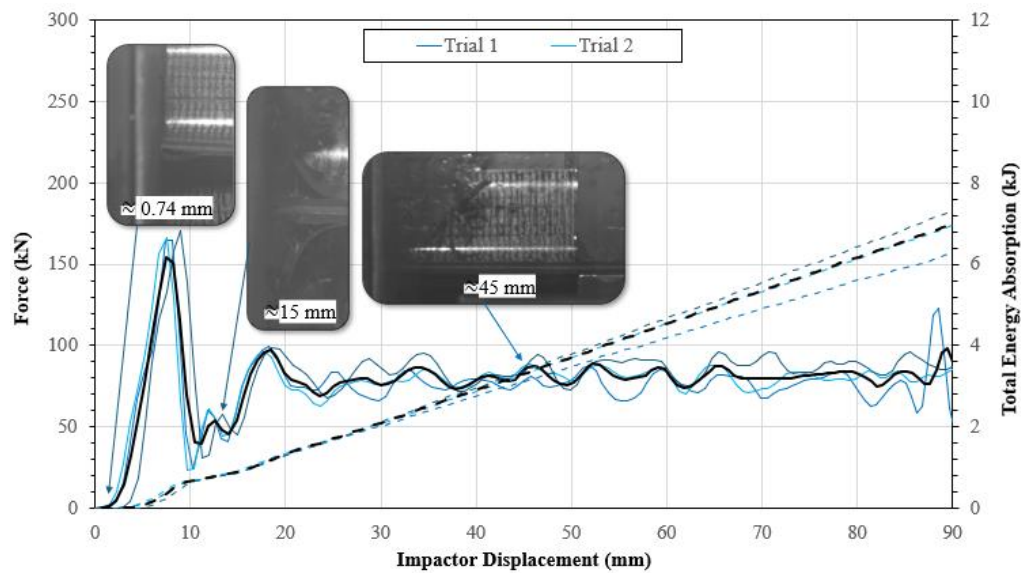


Figure 5.14: Force-displacement response of $[0/\pm 45/90]_s$ double-hat channels under dynamic axial loading.

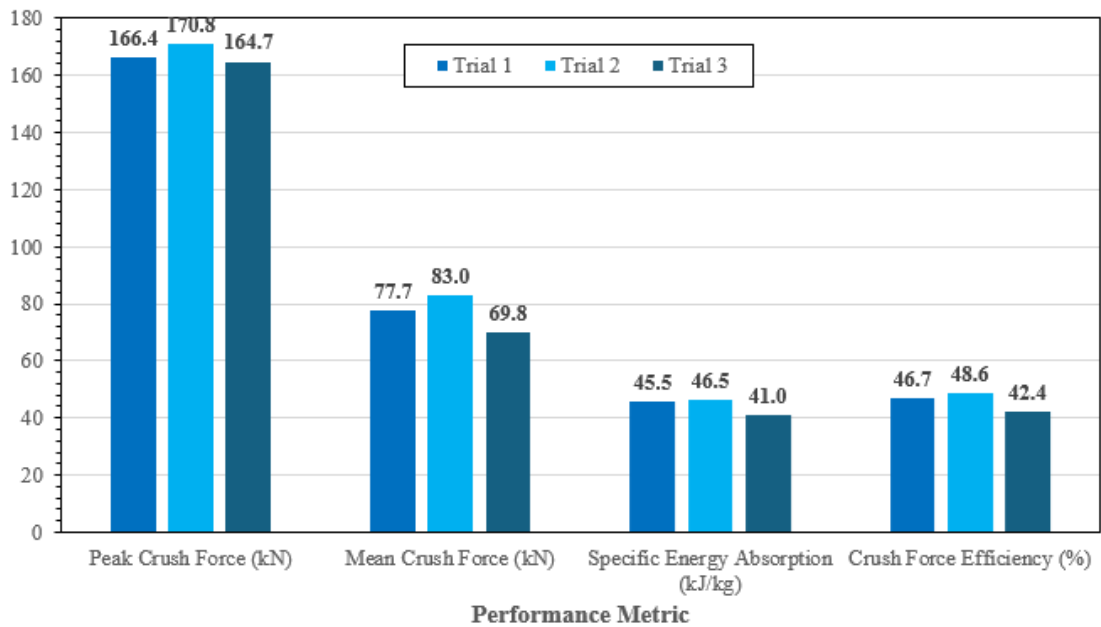


Figure 5.15: Performance metrics of $[0/\pm 45/90]_s$ double-hat channels under dynamic axial loading.

Under dynamic axial loading, the predominant failure mode of the $[0/\pm 45/90]_s$ double-hat channels was fragmentation (Figure 5.16). Short interlaminar cracks near the crush fronts and fibre

fracture amongst the $\pm 45^\circ$ and 90° plies were visible. As was observed with the tapered channels of the same stacking sequence, delamination cracks propagated between the outer 0° and 45° plies which created long fronds. The inner 0° plies, which were constrained by the adhesive, did not de-bond and cohesive failure of the adhesive was not observed ahead of the crush front. Brittle fracture of the internal plies was also visible.

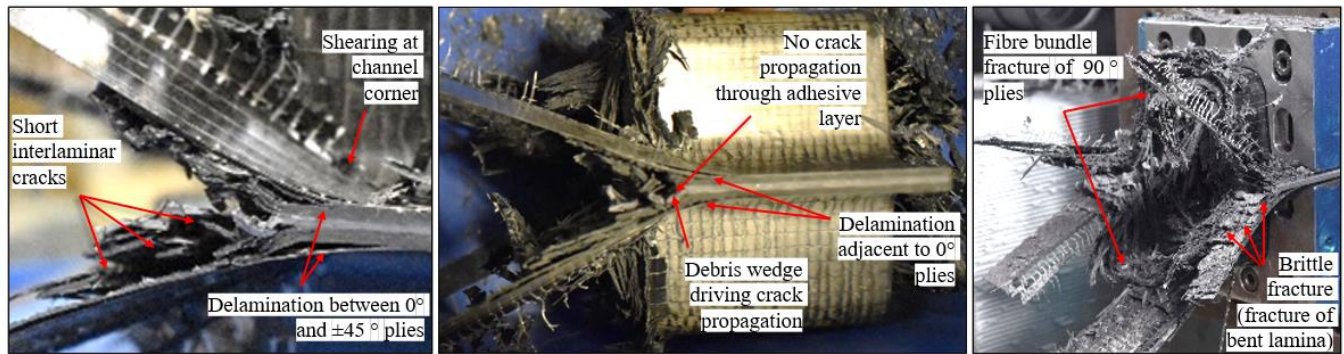


Figure 5.16: Images of the $[0/\pm 45/90]_s$ double-hat channels after dynamic axial loading exhibiting the macroscopic failure modes, brittle fracture and fragmentation.

A representative progression of stable crushing was captured during the tests via the high-speed cameras (Figure 5.17). The images revealed that the adhesive initially de-bonded from the lower adjacent ply locally. However, the speed of loading was such that the crack opening was not ahead of the crush zone for long. As a result, the de-bonding crack was closed and longitudinal cracks formed which eventually led to the development of 0° fronds.

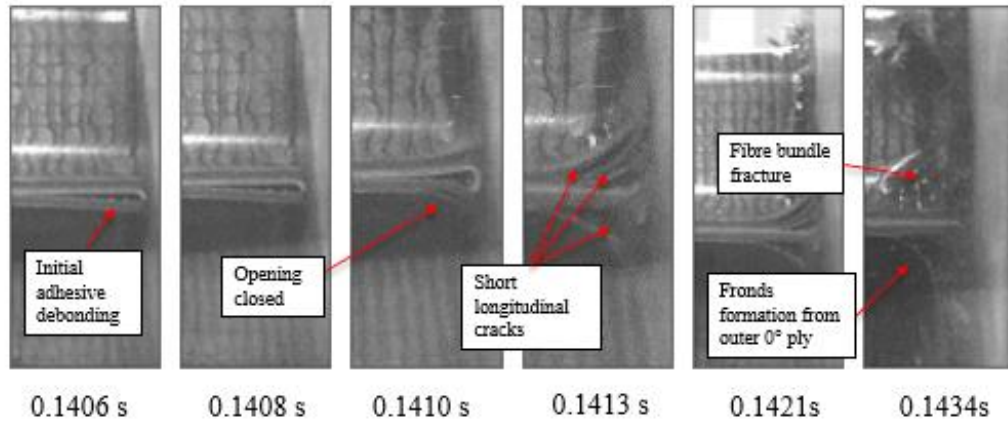


Figure 5.17: Representative initial damage propagation in the $[0/\pm 45/90]_s$ double-hat channel subject to dynamic axial loading

5.3.2 $[\pm 45/0_2]_s$ Double-hat Channels

For the $[\pm 45/0_2]_s$ double-hat channels, there was an average crush force of 109.9 ± 1.3 kN, an average peak force of 252.6 ± 4.8 kN, an average total energy absorption of 9.89 ± 0.1 kJ, an average specific energy absorption of $64.3 \pm 0.5 \frac{\text{kJ}}{\text{kg}}$, and an average crush force efficiency of $43.5 \pm 0.3\%$ (Figure 5.18 and Figure 5.19).

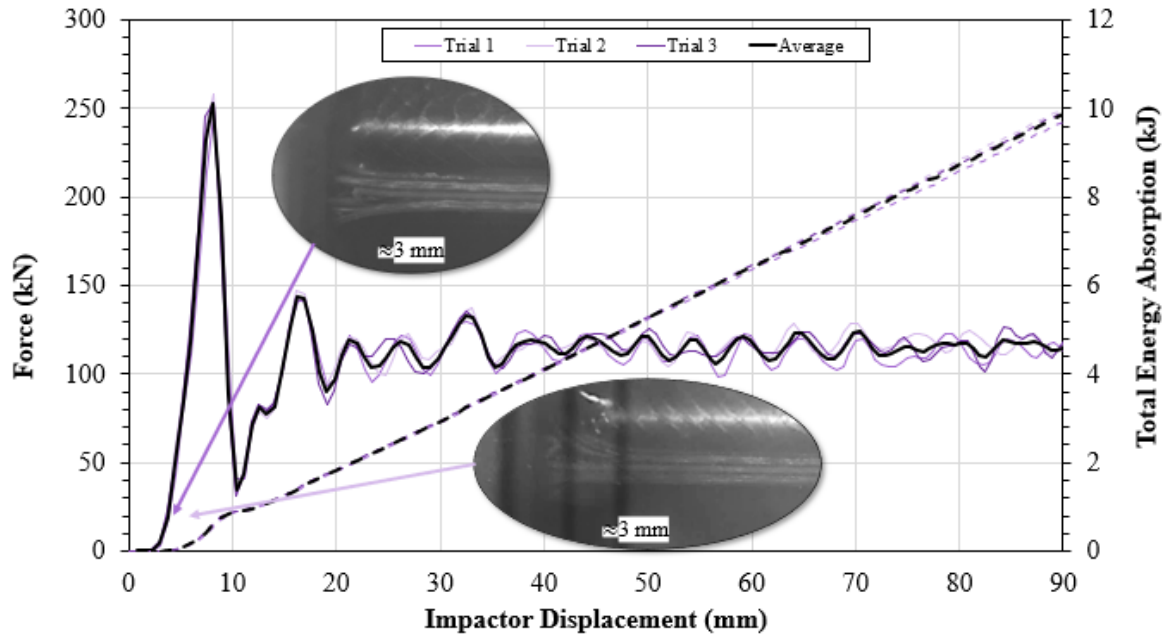


Figure 5.18: Force-displacement response of $[\pm 45/0_2]_s$ double-hat channels under dynamic axial loading.

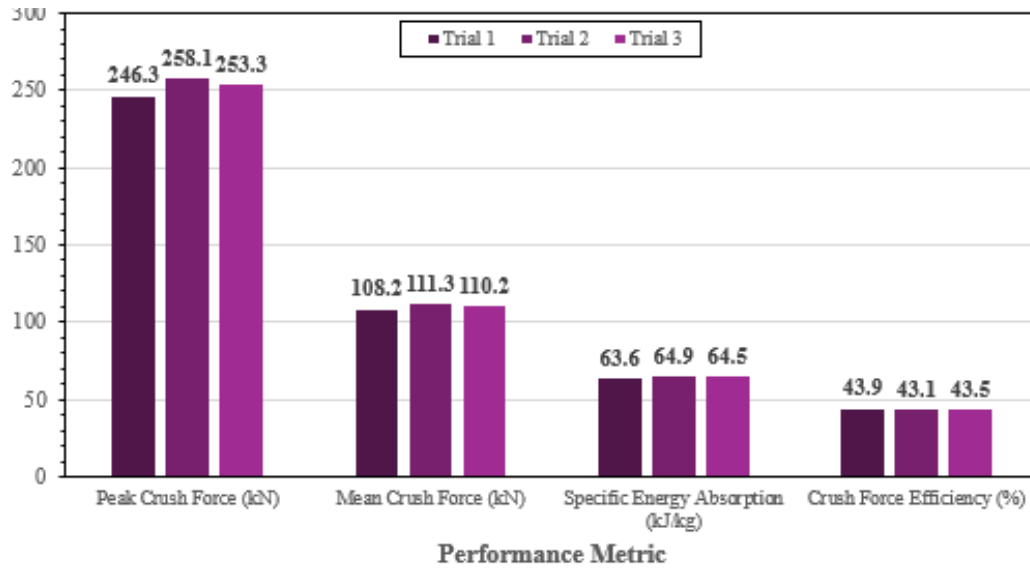


Figure 5.19: Performance Metrics of $[\pm 45/0_2]_s$ double-hat channels under dynamic axial loading.

The predominant failure mode observed for the $[\pm 45/0_2]_s$ double-hat channels was brittle fracture (Figure 5.20). This was characterized by short bent fronds with significant transverse shearing and fibre fracture. Debris wedges driving crack propagation between the two channels and along the adhesive were also observed. However, the adhesive itself did not de-bond or prematurely fracture ahead of the crush front.

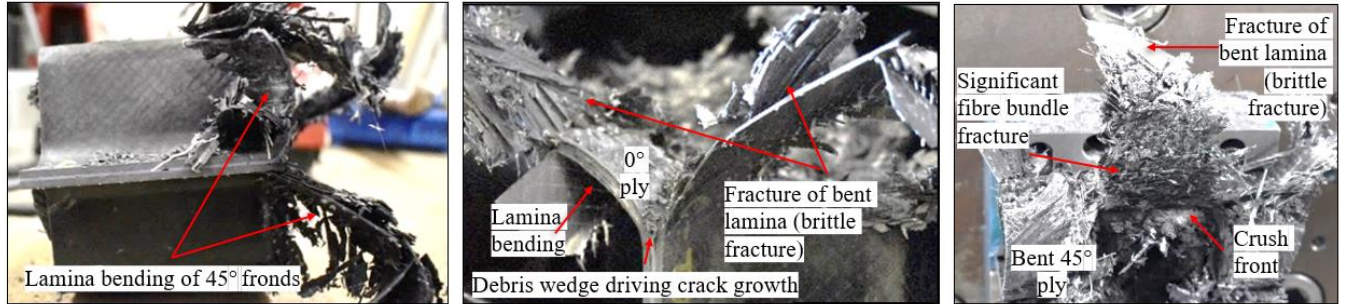


Figure 5.20: Images of the $[\pm 45/0_2]_s$ double-hat channels after dynamic axial loading exhibiting the macroscopic failure, brittle fracture.

From the high-speed images, more delamination at the onset of crushing was observed with the $[\pm 45/0_2]_s$ channels than with the $[0/\pm 45/90]_s$ channels. Additionally, fibre-kinking as a result of compression was quickly observed and resulted in the fracture of fronds (brittle fracture). The delamination of the outer plies would eventually result in the larger fronds also observed early on.

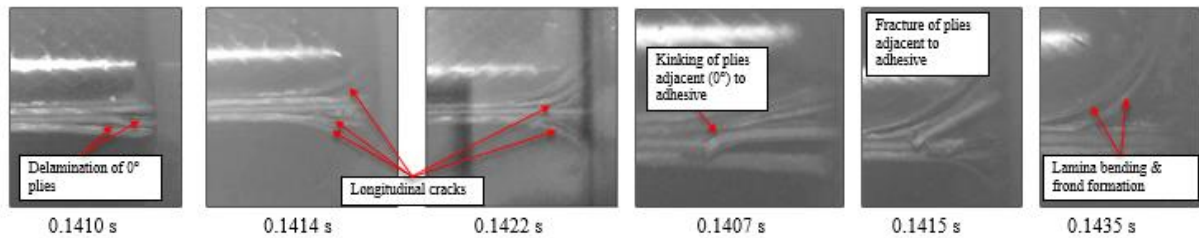


Figure 5.21: Representative initial damage propagation in the $[\pm 45/0_2]_s$ double-hat channels subject to dynamic axial loading.

5.4 Quasi-Static Axial Loading of Double-Hat Channels

The experimental results of the quasi-static axial loading of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ double-hat channels are summarized and discussed below.

5.4.1 $[0/\pm 45/90]_s$ Double-Hat Channels

For the $[0/\pm 45/90]_s$ double-hat tests, there was an average crush force of 100.5 ± 5.7 kN, an average peak force of 129.5 ± 9.8 kN, an average total energy absorption of 8.7 ± 0.5 kJ, an average specific energy absorption of $59.9 \pm 3.3 \frac{\text{kJ}}{\text{kg}}$, and an average crush force efficiency of $77.9 \pm 5.1\%$ (Figure 5.22 and Figure 5.23). For this set, four samples were tested because the first sample prematurely de-bonded but continued to progressively crush with increasing impactor displacement (Appendix F). Besides this, the trials were consistent in their force-displacement responses. However, it was observed that cracks between the plies and adjacent to the adhesive propagated ahead of the crush front.

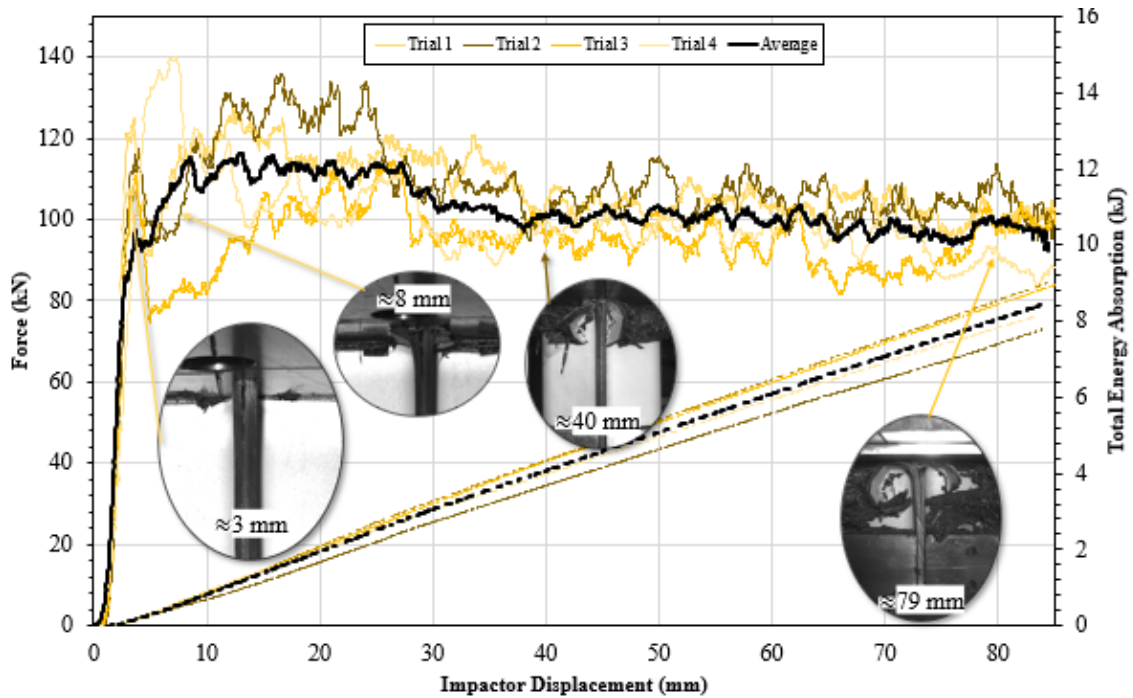


Figure 5.22: Force-displacement response of $[0/\pm 45/90]_s$ double-hat channels under quasi-static axial loading.

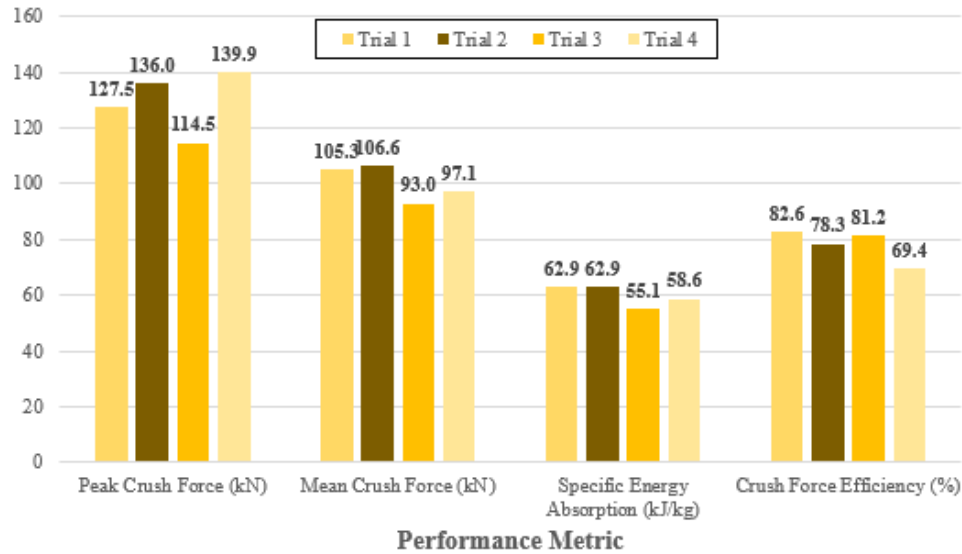


Figure 5.23: Performance Metrics of $[0/\pm 45/90]_s$ double-hat channels under quasi-static axial loading.

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ double-hat channels, it was observed that in contrast to their performance under dynamic axial loading, the predominant failure modes were brittle fracture (a combination of fragmentation and lamina bending) (Figure 5.24). Additionally, the crushed material was amalgamated within the centre of the channel as crushing progressed. The outer 0° plies remained and exhibited significant bending, while the inner plies fractured along the bent fronds. Debris wedges drove progressive crushing between a 90° and -45° degree layer. Lastly, delamination ahead of the crush front until the end of loading was observed.

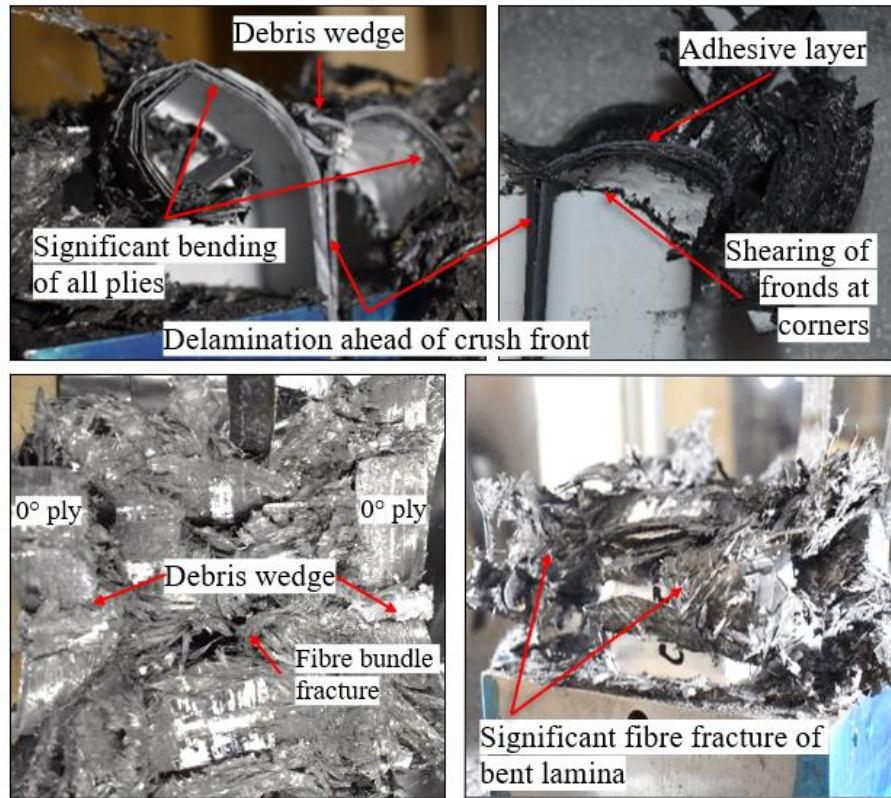


Figure 5.24: Images of the $[0/\pm 45/90]_s$ double-hat channels after quasi-static axial loading exhibiting the general failure mode, brittle fracture.

5.4.2 $[\pm 45/0_2]_s$ Double-Hat Channels

For the $[\pm 45/0_2]_s$ double-hat channels, there was an average crush force of 109.7 ± 6.8 kN, average peak force of 167.0 ± 13.4 kN, average total energy absorption of 9.7 ± 0.04 kJ, average specific energy absorption of $64.7 \pm 1.5 \frac{\text{kJ}}{\text{kg}}$, and an average mean crush force efficiency of $65.7 \pm 8.3\%$ (Figure 5.25 and Figure 5.26). A decreasing mean crush force as opposed to a stable mean crush force was also more pronounced within the force-displacement responses of the $[\pm 45/0_2]_s$ channels and is further discussed in section 5.5. Beyond this, the formation of a debris wedge made with the adhesive material as well as the split of the double-hat channel into two major fronds was observed.

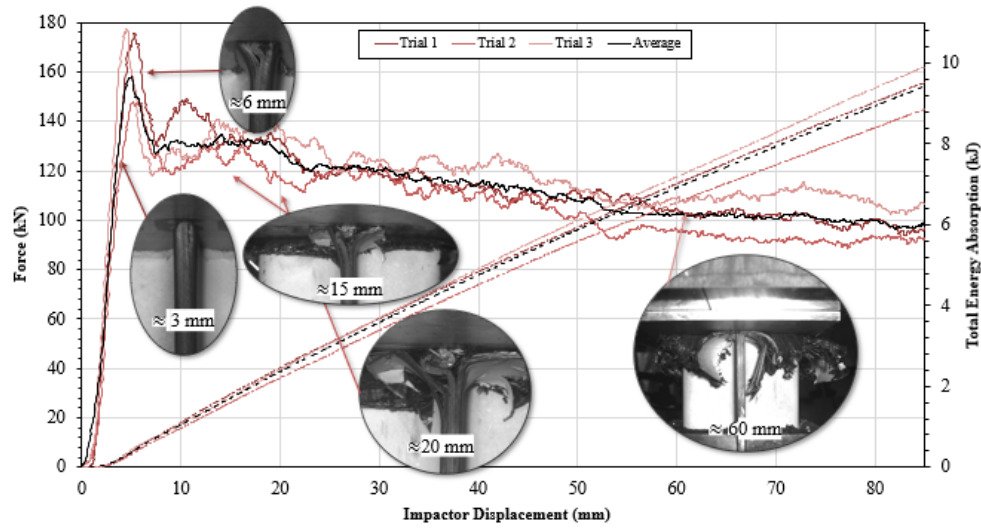


Figure 5.25: Force-displacement response of $[\pm 45/0_2]_s$ double-hat channels under quasi-static axial loading.

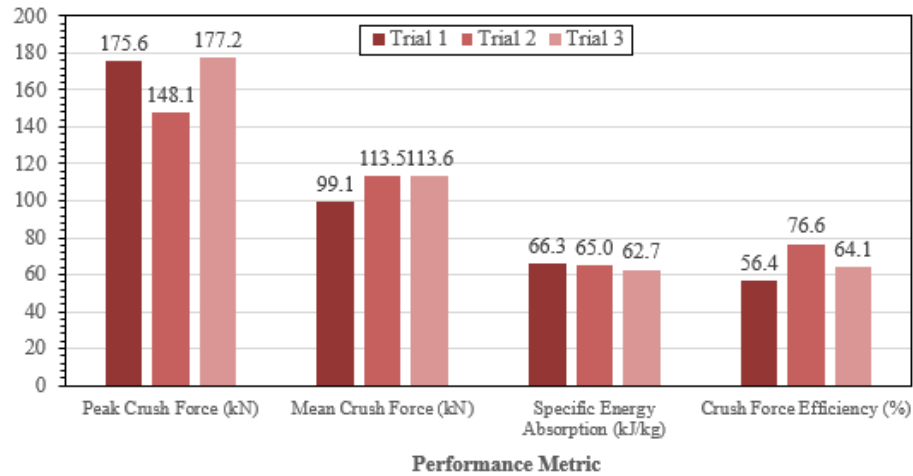


Figure 5.26: Performance metrics of the $[\pm 45/0_2]_s$ double-hat channels under quasi-static axial loading.

For the quasi-static axial loading of $[\pm 45/0_2]_s$ double-hat channels, the predominant failure mode exhibited was splaying/lamina bending with some brittle fracture (Figure 5.27). It was observed that the tows of the 0° plies remained intact while the innermost plies ($\pm 45^\circ$) fractured in a brittle manner due to shearing. The outermost plies (also $\pm 45^\circ$) remained mostly intact as well because the debris wedge enabling macroscopic crack propagation through the specimen existed a ply away from the adhesive (within the inner $\pm 45^\circ$ plies). Thus, the outer laminae bent except at the corners of the

channel, where shearing occurred at the channel wall. It was also observed that the material did not tend to accumulate within the channel as it did with the previous stacking sequence. This was first because there was less fragmentation with this stacking sequence and thus less material to gather. Secondly, it was observed that there was a layer of lamina which lost stiffness as a result of significant matrix cracking and thus folded inward along the walls of the channel.

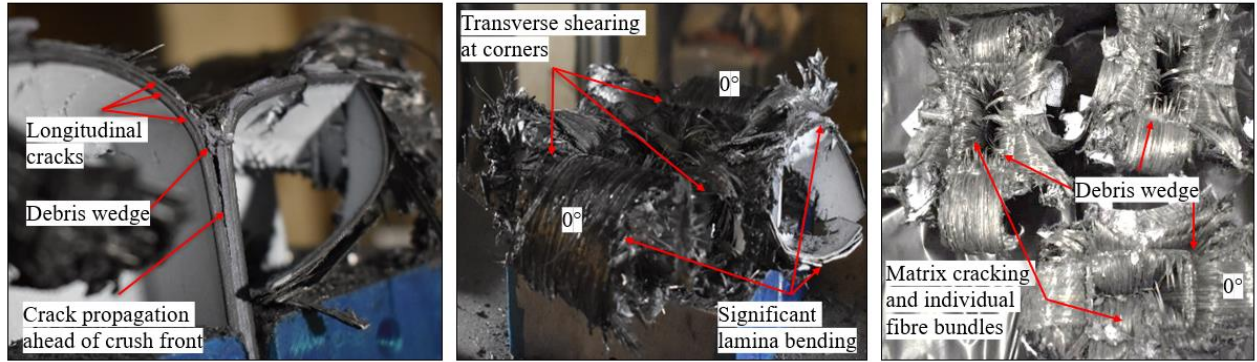


Figure 5.27: Images of the $[\pm 45/0_2]_s$ double-hat channels after quasi-static axial loading exhibiting the macroscopic failure, splaying and some brittle fracture.

5.5 Discussion of Double-Hat Channel Experimental Results

Based on the experimental results of axially loaded double-hat channels, the use of an adhesive to bond two channels did not improve the performance of the components. However, in some cases, the predominant failure modes were altered, and energy absorption was reduced.

5.5.1 Performance of Double Hat Channels per Stacking Sequence and Loading Rate

When comparing the effect of stacking sequence, the $[\pm 45/0_2]_s$ channels were once again more efficient energy absorbers than the $[0/\pm 45/90]_s$ channels (though the difference was small under quasi-static loading). This was attributed to reasons mentioned previously (i.e. splaying which tends toward delamination and friction). The average peak force of the $[\pm 45/0_2]_s$ was 22% higher than the $[0/\pm 45/90]_s$ under quasi-static testing. However, the average mean crush force and average specific energy absorption differed by 7.5% and 1.7%, respectively (Figure 5.28).

In contrast, more significant differences between the axial loading metrics were observed amongst the samples loaded dynamically (Figure 5.29). Though the $[\pm 45/0_2]_s$ channels still presented higher peak crush forces, the difference in contrast to the $[0/\pm 45/90]_s$ channels was 34%, with the

difference for average mean crush force and SEA being 30% and 32%, respectively. Thus, the influence of the damage mechanisms responsible for absorbing energy was more pronounced under dynamic loading than under quasi-static conditions. This was further supported by the failure modes identified in Figure 5.24 and Figure 5.27, which indicated brittle fracture as a present failure mode in both stacking sequences under quasi-static loading.

The mean crush force of the $[\pm 45/0_2]_s$ channels under quasi-static loading exhibited a decreasing mean crush force, which was also observed with single hat channels of the same geometry, stacking sequence and loading condition tested by Harvey [25]. This can be explained by the reduction in the cross-sectional area as the channel underwent crushing. Whereas with the alternative stacking sequences, much of the fragmented material was restricted to within the channel and was progressively crushed with the rest of the channel. For the $[\pm 45/0_2]_s$ channels, the material was on the outside as fronds and was not continuously crushed during the experiment. The innermost plies were fragmented, and the cross-section in contact with the platen was reduced (i.e., as can be seen Figure 5.27, the innermost plies were entirely crushed). As a result, eventually, only about half of the plies (half the thickness of the cross-section) were in contact with the platen under crushing. As a result of this phenomenon, defining specific energy absorption based on mass “crushed” where the crushed mass is a function of the distance travelled by the impactor from initial contact, can be a misleading metric because there was a significant proportion of mass that was never crushed due to lamina bending.

When comparing the effect of loading rate only, specific energy absorption was greater in the quasi-static loading case than in the dynamic condition for the $[0/\pm 45/90]_s$ channels, while being similar for the $[\pm 45/0_2]_s$ channels. Firstly, this indicated that the stacking sequence was far more influential than other variables when considering the energy-absorbing capacity of a bonded channel. For the quasi-isotropic stacking sequence, quasi-static loading facilitated an increased density of delamination and lamina bending, which aided in energy absorption [24]. This phenomenon was less possible under dynamic conditions because of the speed at which the crush front progressed through the channel, which resulted in shearing and fibre fracture. However, for the $[\pm 45/0_2]_s$ channels, delamination remained the prominent energy-dissipating mechanism regardless of loading rate. Thus, the difference between dynamic and quasi-static performance can be attributed to the strain rate

dependency of the matrix, which facilitates increased delamination (and thus splaying) at quasi-static rate, but fragmentation at dynamic rates.

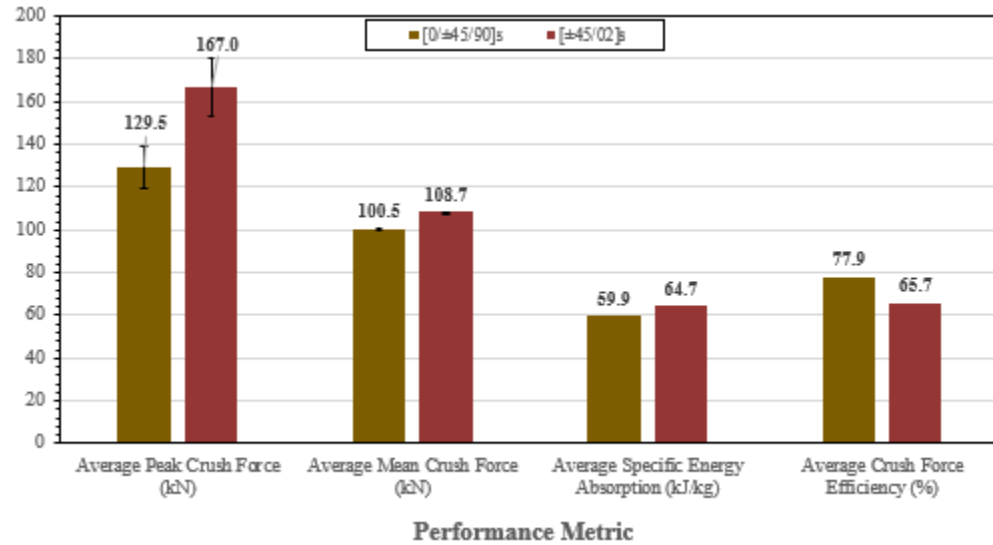


Figure 5.28: Comparison of performance metrics for [0/±45/90]_s and [±45/0₂]_s quasi-static axial loading of double-hat channels. The scatter of the experimental data, notably for the latter two metrics, was minimal.

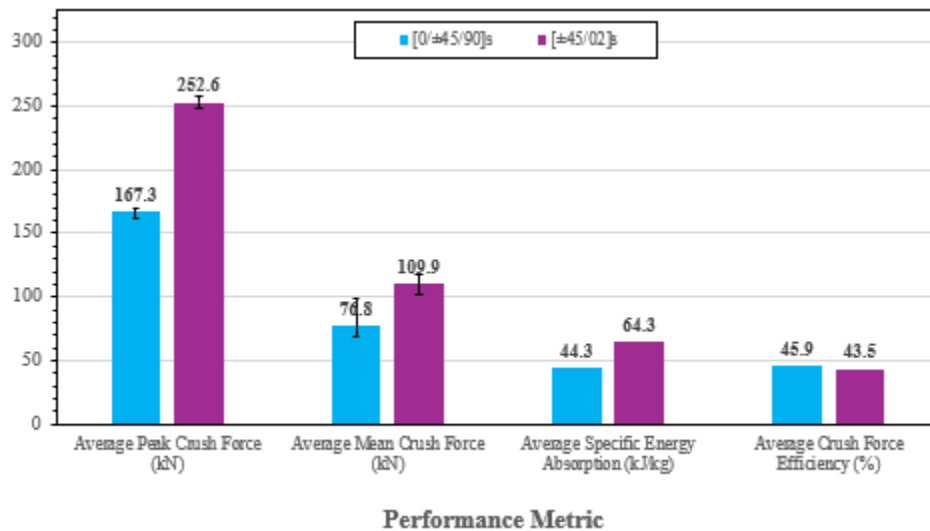


Figure 5.29: Comparison of performance metrics for [0/±45/90]_s and [±45/0₂]_s dynamic axial loading of double-hat channels. The scatter of the experimental data, notably for the latter two metrics, was minimal.

5.5.2 Comparison of Double-Hat Channel Responses to Hat Channel Responses of the Same Stacking Sequence

It was also useful to compare the performance of the double-hat channels to previously tested open channels of similar geometry, stacking sequence, and loading condition. The performance metrics were determined from experimental tests performed by Harvey [25]. For all combinations of loading conditions and stacking sequences, the specific energy absorption of the double-hat was lower than that of the corresponding hat channel.

Firstly, this may be explained by the crush front of each set of geometry. In the double-hat channels, the plies adjacent to the adhesive layer and the adhesive layer itself form the debris wedge and drive the two channels apart. In just a hat channel, a debris wedge must be formed between two plies. It is thus suggested by these results that more energy was required (dissipated) through the formation of a debris wedge and crack propagation between two plies than through crack propagation between two channels.

Additionally, imperfections were introduced in the trimming and chamfering of the channels manually, which may have led to differences in the failure modes triggered and the assumptions about the amount of mass crushed (trimming of flanges). Lastly, as discussed in the methodology, it was necessary to print fixtures to ensure that the two channels would be aligned/symmetrical to one another during the curing of the adhesive. Before the fixtures were implemented, misalignment between the two hat channels forming a double-hat channel was observed. This may have had a more pronounced effect in the $[\pm 45/0]_2$ channels, where there are more 0-degree plies, as opposed to the $[0/\pm 45/90]_2$ channels.

Beginning with the $[0/\pm 45/90]_2$ components subject to dynamic axial loading, it was observed that the peak force and mean force averages were higher for the double-hat channels than the single hat channel under dynamic axial loading (+51%, and +47%, respectively) (Figure 5.30). This was expected because the mass of the channels also approximately doubled with the use of two hat channels instead of one. That being said, the SEA of the double-hat channels averaged 7% lower than the hat channels. Since both geometries exhibited very similar failure modes (Figure 5.31), it is likely that this loss in efficiency was the result of defects introduced during the preparation of the double-hat channel and the aforementioned change in the formation of the debris wedge (as opposed to a

significant difference between the damage mechanisms of the $[0/\pm 45/90]_s$ hat and double-hat channels under dynamic axial crushing.

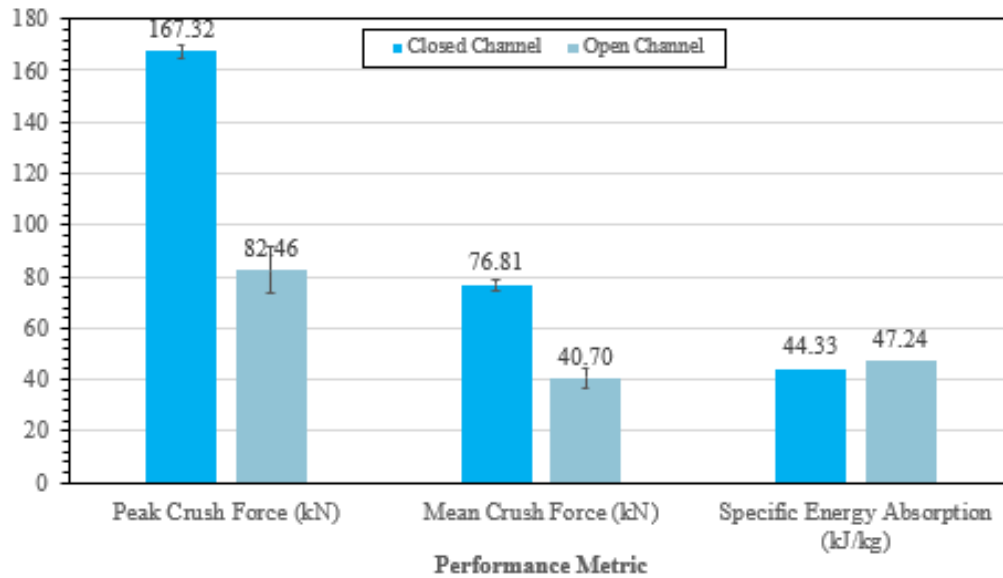


Figure 5.30: Comparison of $[0/\pm 45/90]_s$ hat [76] and double hat channel dynamic axial loading metrics.

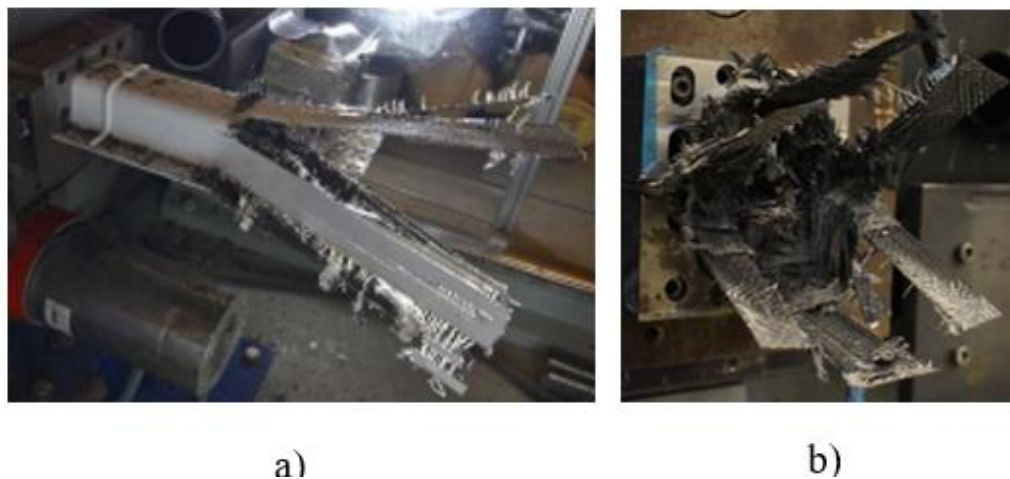


Figure 5.31: Comparison of failure mode of the $[0/\pm 45/90]_s$: (a) hat [25] and (b) double hat channels under dynamic axial loading.

Similar to the previous stacking sequence, for the $[\pm 45/02]_s$ channels, the double-hat geometry averaged a higher peak force and mean crush force, but a lower specific energy absorption

(+58%, +33%, and -33% respectively) (Figure 5.32). Whereas for the previous stacking sequence, there was not an observable difference between the failure modes of the two sets of channels, there was distinctly more splaying and brittle fracture in the $[\pm 45/0_2]_s$ double-hat channel tests under dynamic axial loading (Figure 5.33). Where such behaviour has thus far indicated superior energy-absorbing capabilities, it does not do so with this set of data. This suggests that less energy is required to generate the opening fracture mode demonstrated by the double-hat channels than the fragmentation achieved by shearing the plies of the open hat channels.

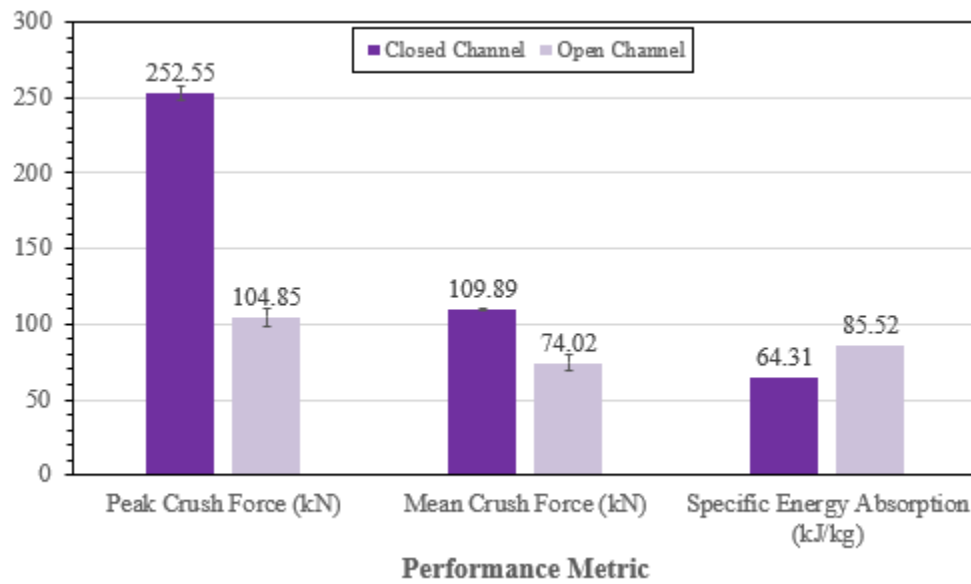


Figure 5.32: Comparison of $[\pm 45/0_2]_s$ [25] and double-hat channel dynamic axial loading performance metrics.

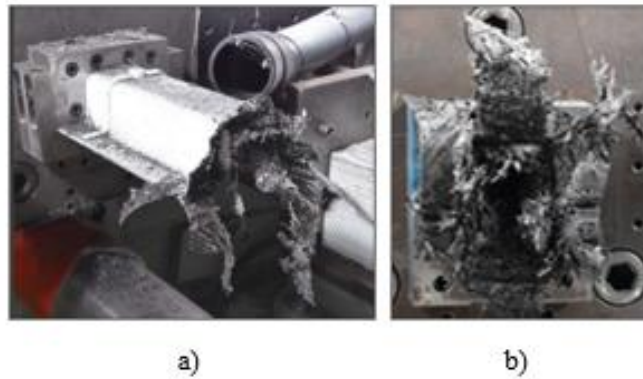


Figure 5.33: Comparison of failure mode of the $[\pm 45/0_2]_s$ (a) hat [25] and (b) double-hat channels under dynamic axial loading.

In contrast to the dynamic results, there was a +49% change in peak force, +44% change in mean crush, and -8% change in SEA for the quasi-static axial loading of the $[0/\pm 45/90_s]_s$ double-hat channels (Figure 5.34). This improvement in performance can be explained via the reduction in fragmentation demonstrated by the double-hat channels when contrasted with hat channels of the same stacking sequence, tested with the same loading conditions (Figure 5.35). The change in the failure was the result of the constraint applied by bonding to another channel, which resulted in delamination of the 0° plies instead of shearing the inner angled plies. That being said, the improvement was minimal, which corresponded to the minimal amount of lamina bending that occurred in these channels under quasi-static axial loading.

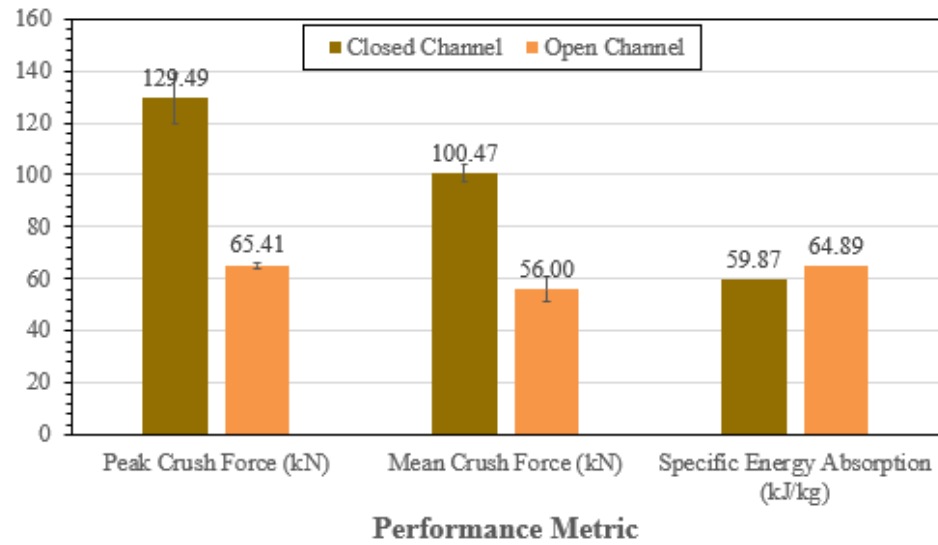


Figure 5.34: Comparison of axial crush metrics of $[0/\pm 45/90]_s$ hat [25] and $[0/\pm 45/90]_s$ double-hat channels under quasi-static axial loading.

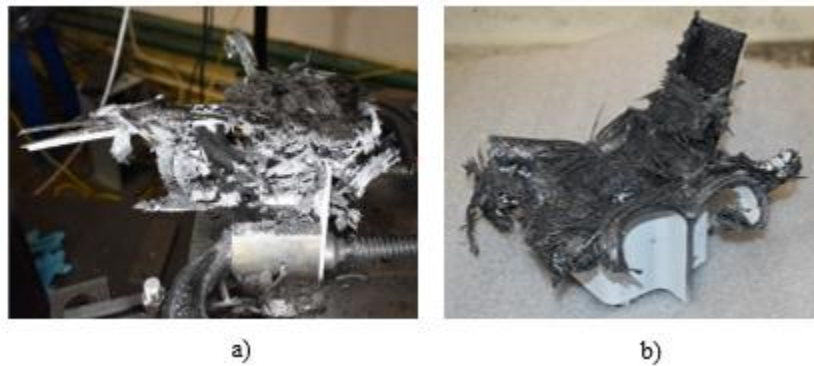


Figure 5.35: Comparison of failure mode of (a) hat [25] and (b) double-hat $[0/\pm 45/90]_s$ hat channels under quasi-static axial loading.

In the case of the $[\pm 45/0_2]_s$ hat and double-hat channels under quasi-static axial loading, the double-hat channels were slightly less efficient in SEA and also exhibited less splaying in their failure modes (+50% peak force, +44% mean crush force, -9% SEA) (Figure 5.36). It is notable, however, that in both cases the force-displacement mean force was not constant [25]. Based on images provided by Harvey [25], there was significant splaying of the lamina as a result of extensive matrix cracking to free individual fibre bundles (Figure 5.37a). Significant splaying and bending was exhibited in the double-hat channels, but a much lesser extent of matrix cracking.

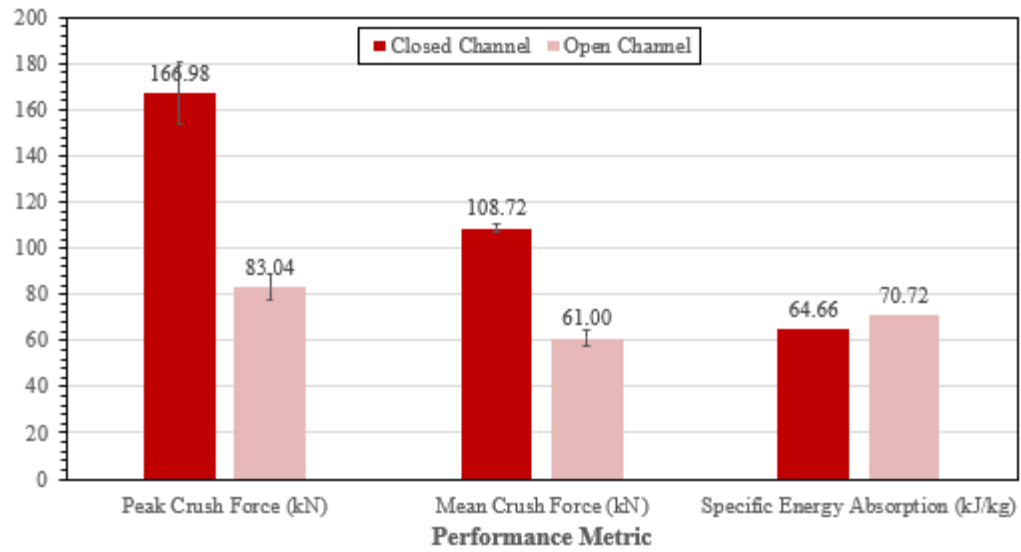


Figure 5.36: Comparison of axial crush metrics of $[\pm 45/0_2]_s$ hat [25] and $[\pm 45/0_2]_s$ double-hat channels under quasi-static axial loading.



Figure 5.37: Comparison of failure mode of the (a) $[\pm 45/0_2]_s$ hat [25] and (b) $[\pm 45/0_2]_s$ double-hat channels under quasi-static axial loading.

Chapter 6

Numerical Modelling Results & Discussion

The force-displacement response and energy absorption predictions of the simulation models described in Chapter 4 are presented and discussed. First, the results of the parametric investigation to calibrate MAT_054 for the simulation model of the baseline condition are presented and the selection of calibrated parameters is discussed (Section 6.1). The results of the simulation models for each subsequent condition (with different geometries, stacking sequences, and loading cases), for which the calibrated MAT_054 was used are then presented (Sections 6.2–6.7). These results are discussed throughout this chapter to highlight the capability and limitations of the simulation models as well as the suitability of MAT_054 in capturing the composite material failure response.

6.1 Calibration of Material Model Parameters for Simulation Model of the Baseline Condition

The results of a parametric study (Table 4.3) to determine the combination of MAT_054 non-physical parameters that would achieve good agreement between the predicted and average experimental force-displacement responses of the baseline condition (dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel) are presented.

6.1.1 Initial Calibration Results for the Simulation Model with an Angled Quadrilateral Mesh to Represent the Hat Channel

Calibration of the material model's non-physical parameters was initially performed for a simulation model with an angled quadrilateral mesh (Appendix D). It was found that with most default values and the appropriate selection of effective failure strain (EFS) (Table 6.1), a force-displacement response indicative of stable progressive crushing could be readily predicted for the baseline condition (Figure 6.1). The force-displacement responses were filtered at 600 Hz SAE as per the recommendation of Feraboli et al. [9]. The responses included a near-zero decrease in force after the failure of the first row of elements, followed by a gradual rise in force as a result of filtering. The initial response underpredicted the performance of the channel, and adjustments to other non-physical parameters were required to increase the mean crush force.

Table 6.1: Initial MAT_054 material model parameters selected for the simulation model of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel with an angled quadrilateral mesh used for the representation of the channel.

Parameter	Value
RO, E_A , E_B , PRBA, G_{AB} , G_{BC} , G_{CA}	Experimentally derived material properties as per Table 3.1
DFAILM, DFAILC, DFAILS, DFAILT	Overridden by EFS.
EFS	0.34
TFAIL	2.5E-07 ms
ALPH	0.1
SOFT	0.50
FBRT	0.5
YCFAC	2.0
XC, XT, YC, YT, SC	Experimentally derived material strengths as per Table 3.1
BETA	0.5
PEL	100%
SLIMT1, SLIMT2, SLIMC1, SLIMC2, SLIMS	1.0

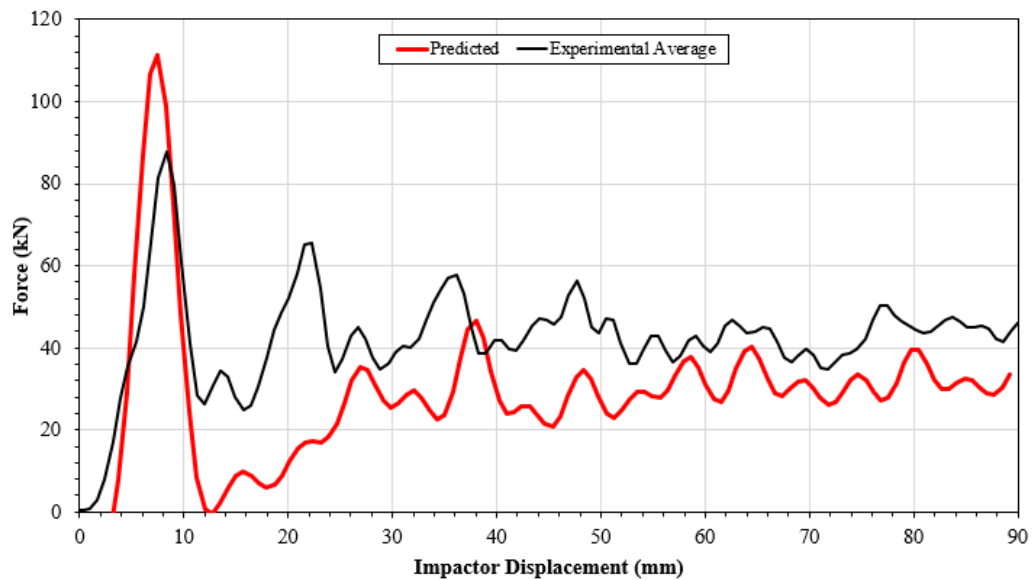


Figure 6.1: Predicted and experimental [76] force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using values of MAT_054 outlined in Table 6.1 and an angled quadrilateral mesh for the hat channel representation.

To adjust the mean crush force predicted, the SOFT parameter (scale factor for the strength of the elements adjacent to the crash front) was adjusted until a mean crush value close to the

experimental average was achieved. Increases in SOFT led to near-proportional increases in the mean crush force without altering the peak force (Figure 6.2). Given that the experimental average for the mean crush force was 40.7 kN, a SOFT value of 0.75 was chosen.

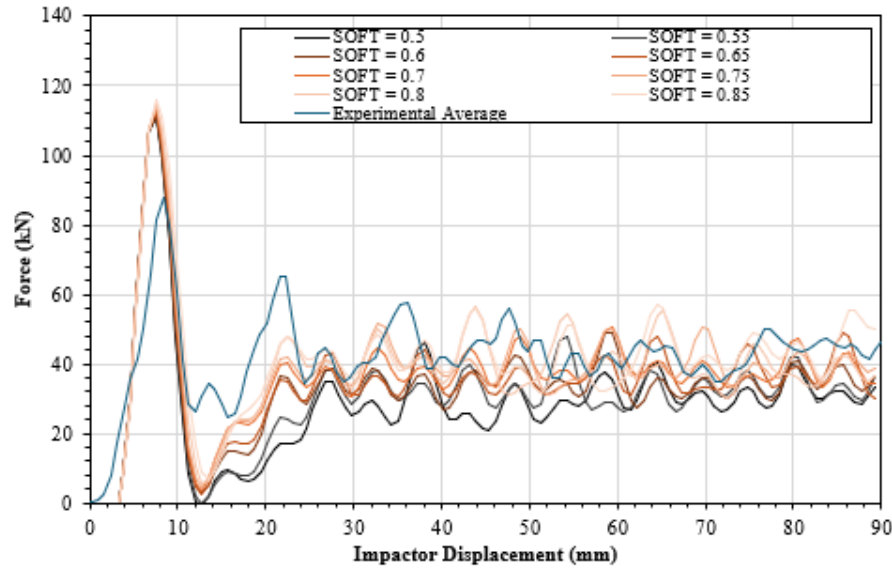


Figure 6.2: Influence of SOFT on the predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using Table 6.1 for MAT_054, and an angled quadrilateral mesh. The average experimental force-displacement response is presented in blue [76].

Next, as proposed by Feraboli and Wade [39], changing the thickness of the trigger can act as a softening factor for the first row of elements and proportionally change the peak force. Thus, simulation models with ply thicknesses between 0.19 and 0.19 mm were created to determine the thickness that would best approximate the experimental results (Figure 6.3). The results were again filtered with a 600 Hz SAE filter. Note, that the use of the filter was also capable of significantly altering the peak force in the response. A final trigger thickness of 0.125 mm was selected.

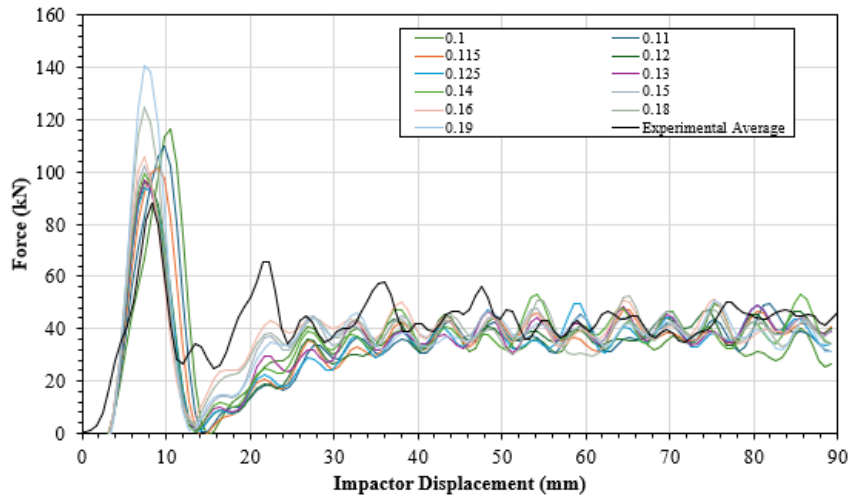


Figure 6.3: Influence of the trigger thickness (mm) on the predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using Table 6.1 for MAT_054, and an angled quadrilateral mesh. The average experimental force-displacement response is presented in black [76].

With the selection of 0.75 for SOFT, 0.125mm for the trigger thickness, and the values in Table 6.1, a peak crush force of 96.90 kN, a mean crush force of 37.25 kN, and a total energy absorption of 7.45 kJ were predicted by the initial simulation model for the first 200 mm of impactor displacement (Figure 6.4). Good agreement with the experimental averages was achieved with deviations of 14.90%, 9.26%, and 9.40% for the peak force, mean crush force, and energy absorption, respectively. The simulation model predicted a fragmentation mode of failure, which was similar to the mode observed experimentally (Figure 6.5). However, non-representative bending of the channel flanges was also predicted.

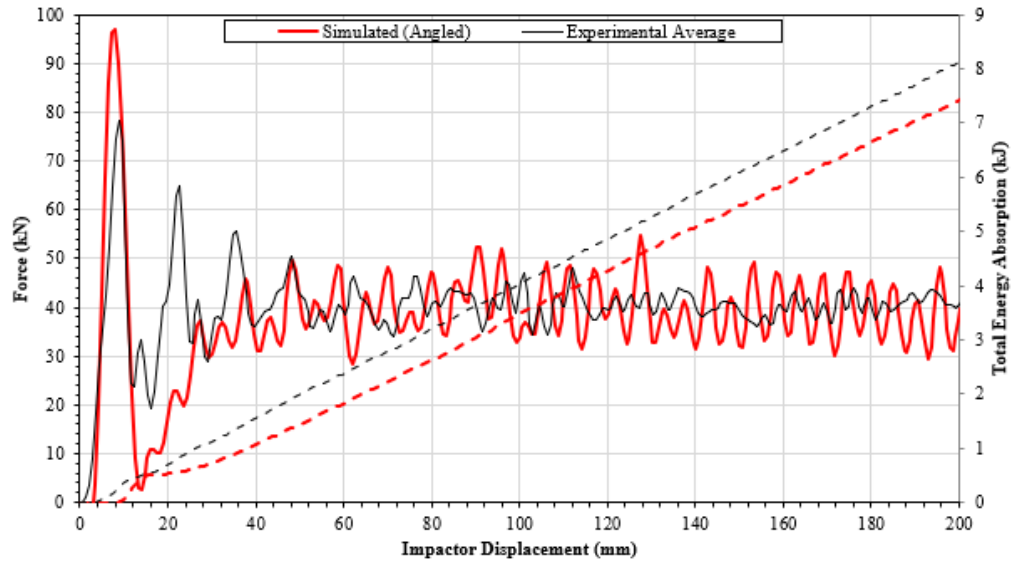


Figure 6.4: Simulation model calibrated for the baseline case: Predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using Table 6.1 for MAT_054, SOFT value of 0.75, trigger thickness of 0.125 mm and an angled quadrilateral mesh to represent the hat channel. Average experimental force-displacement response presented in black [76].

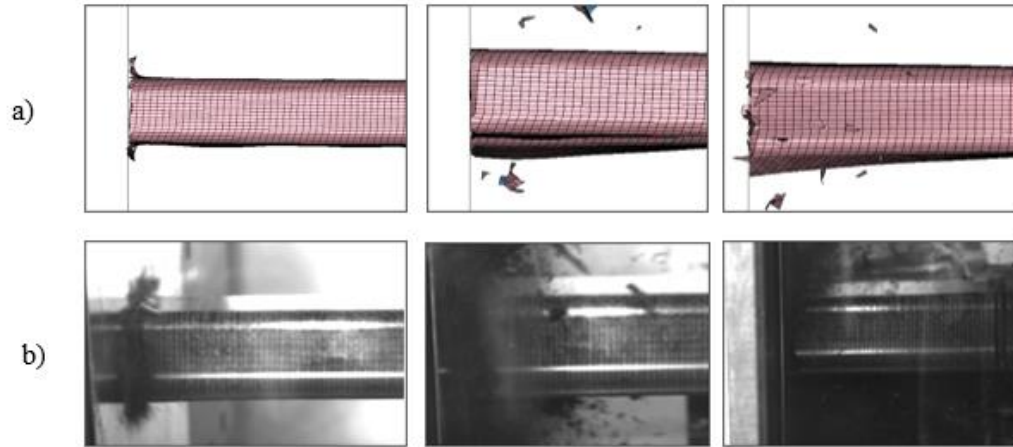


Figure 6.5: Crush behaviour of the $[0/\pm 45/90]_s$ hat channel obtained by (a) simulation model calibrated for the baseline case and (b) experimental dynamic loading [25].

The same calibrated material model parameters were used to predict the force-displacement response of the tapered and corrugated channels with the same stacking sequence under the same

loading conditions. A good agreement was also found for these channels with small caveats. The results are presented and discussed in Appendix D.

6.1.2 Simulation Model Results of a Structured Quadrilateral Mesh Used to Represent the Channel Parts

As mentioned in Section 4.1, a structured quadrilateral mesh was ultimately used to create the channels because of the simplicity of its generation. A similar simulation model to that outlined in Section 6.1.1 but with a structured quadrilateral mesh to represent the hat channel was created. The corresponding predicted force-displacement response with a 300 HZ SAE filter applied is presented (Figure 6.6). The lower filter was selected because of the increased amplitudes of oscillation introduced in the force-displacement response when using a structured quadrilateral mesh. Stable progressive crushing was not achieved with this simulation model. Instead, the channel was predicted to fail prematurely at the end constrained to represent the fixture (Figure 6.6). Thus, recalibration of the material model for this mesh structure was required.

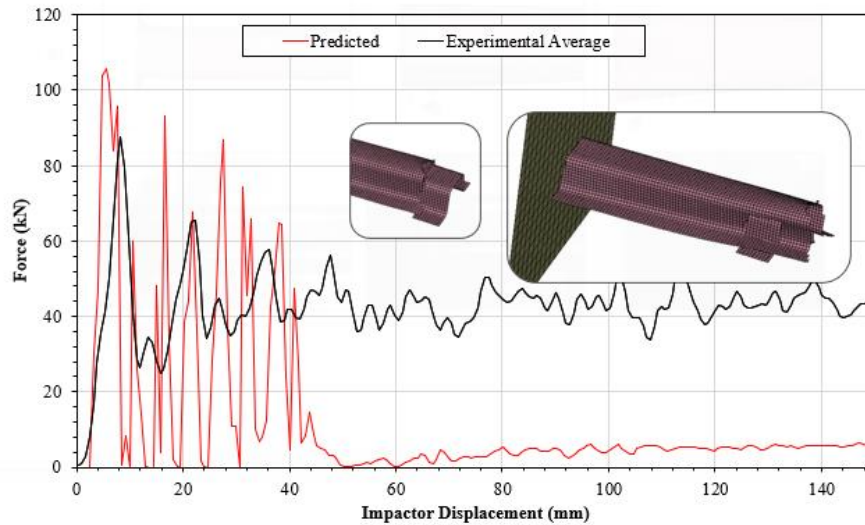


Figure 6.6: Predicted and average experimental [76] force-displacement response and behaviour of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using structured quadrilateral mesh (MAT_054 values previously calibrated for simulation model with angled quadrilateral mesh).

Given the unstable behaviour predicted, the effective failure strain (EFS) value selected was incrementally increased in subsequent simulation models in an attempt to achieve stable progressive

crushing (Figure 6.7). However, no value of EFS trialled resulted in stable crushing (Figure 6.7). Additionally, changes in SOFT as well as other nonphysical parameters in conjunction with a value defined for EFS did not lead to stable progressive crushing predictions either. As a result, it was necessary to consider the use of the DFAILM, DFAILC, DFAILT, and DFAILS parameters (DFAIL_x). These parameters represent the ply failure strains associated with individual deformation modes.

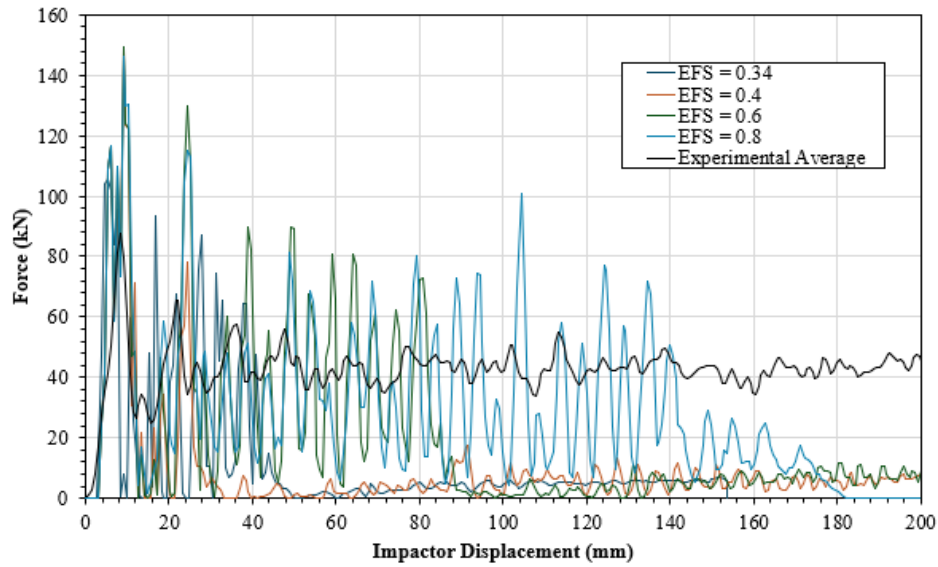


Figure 6.7: Influence of EFS on the predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using a structured quadrilateral mesh to represent the hat channel. The average experimental force-displacement response is presented in black [76].

6.1.3 Recalibration of Material Model Parameters for the Simulation Model of the Baseline Condition with a Structured Quadrilateral Mesh Used to Represent the Channels

6.1.3.1 Effect of DFAIL_x Parameters

To utilize the DFAIL_x parameters, all remaining material model parameters used for the previous simulation model were kept constant (Table 6.1) while EFS was not used. The experimentally derived values for failure strains (Table 3.1) were set as the initial DFAIL_x parameters. Given that lower values of EFS resulted in the prediction of catastrophic failure (Figure 6.7), it was unsurprising that these initial DFAIL_x parameters (Table 6.2) also resulted in the prediction of premature, catastrophic fracturing (Figure 6.8).

Table 6.2: Experimentally derived values of failure strain inputted for the DFAILx parameters in MAT_054 for calibration of the material model with a structured quadrilateral mesh representing the hat channel.

Parameter	Value (mm/mm)
DFAILM	0.0242
DFAILS	0.088
DFAILT	0.0066
DFAILC	-0.008
EFS	0

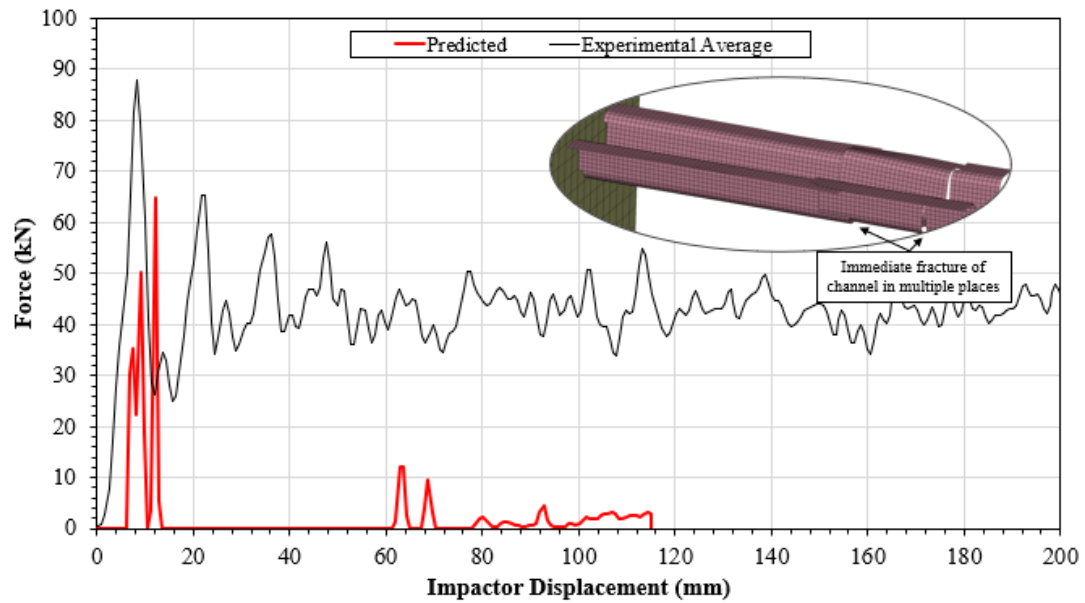


Figure 6.8: Predicted and average experimental [76] force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel with experimentally derived failure strains (DFAILx) inputted in MAT_054 to replace EFS.

The initial failure strain values were scaled in an attempt to achieve a mean crush force of 40.70 kN as per the associated experimental results [25] (Table 6.3, Figure 6.9). Mean crush force was prioritized because this performance metric is proportional to the energy-absorbing capacity of the channel. Five additional simulation models, focused on scaling each parameter individually, and then altogether were created in an attempt to isolate the most influential parameter(s).

Table 6.3: Iterations of the simulation model with a structured quadrilateral mesh representing the hat channel to scale the DFAILx parameters for calibration of MAT_054.

	<u>Iteration</u>				
	A	B	C	D	E
DFAILM (mm/mm)	0.242	0.0242	0.0242	0.242	0.0242
DFAILS (mm/mm)	0.88	0.88	0.088	0.088	0.088
DFAILT (mm/mm)	0.066	0.0066	0.066	0.0066	0.0066
DFAILC (mm/mm)	-0.08	-0.008	-0.008	-0.008	-0.08

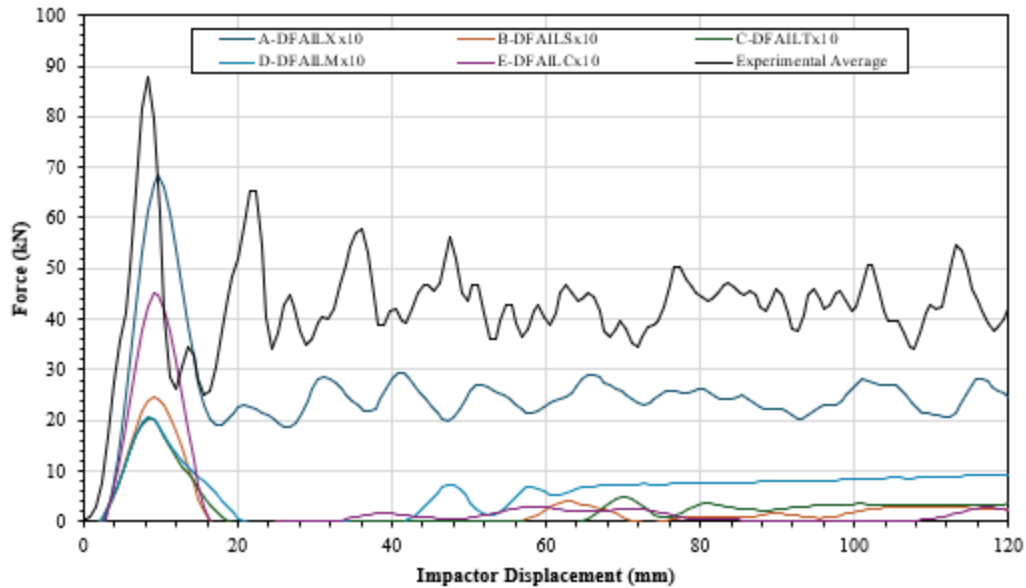


Figure 6.9: Influence of scaling DFAILx parameters by 10 on the predicted force-displacement response of the dynamic axial loading of the $[0/\pm/45/90]_s$ hat channel. Average experimental force-displacement response is in black [76].

While stable crushing was achieved with trial A, the targeted mean crush force was not achieved even when all failure strains were increased by a factor of 10. Thus, the individual DFAIL parameters had to be further scaled. Through additional parametric investigation, it was found that the ability of the simulation model to predict progressive crushing instead of premature fracture depended on the *DFAILT* parameter. Under dynamic axial loading, the simulation models predicted the global

bending of the channel as a whole, a response also observed experimentally but to a lesser extent. Like any beam in bending, this caused the development of high tensile strains in the topmost plies at the fixed end of the channel (i.e., the bottom 50 mm constrained in all degrees of freedom).

Using the history variables available with MAT_054, the time when each integration point within an element experienced a type of failure mode was recorded and made available (Figure 6.10). The failure modes of the integration points (plies) within an element fixed in all six degrees of freedom illustrated that the 0° plies failed in the compressive fibre failure mode while the six remaining plies failed in the tensile fibre mode. Thus, low values of DFALIT resulted in the deletion of elements after minimal impactor displacement. Increasing DFALIT beyond a value of 0.06 enabled predictions of progressive crushing.

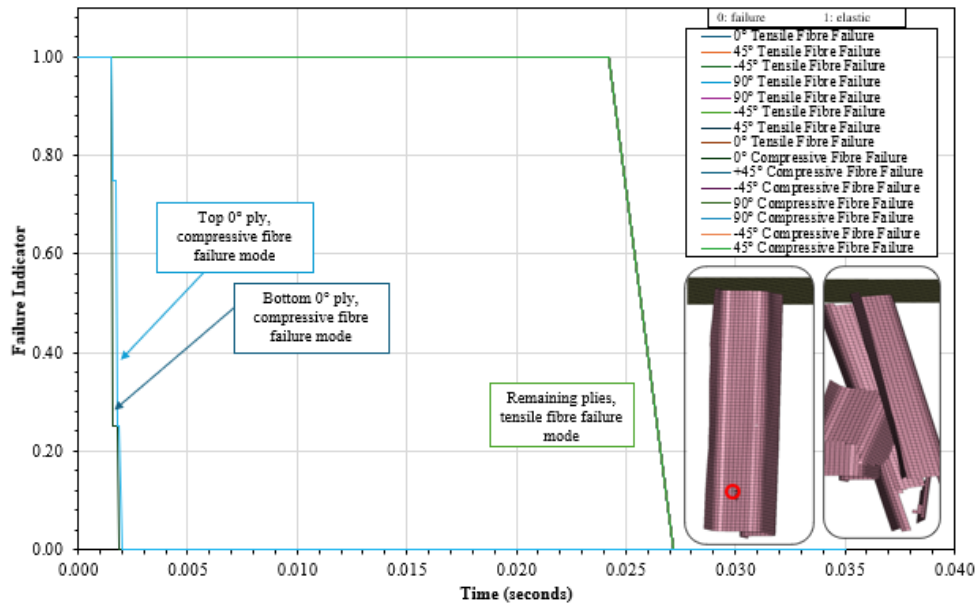


Figure 6.10: Modes of failure recorded per integration point (ply) within an element constrained in six degrees of freedom for the simulation model, Trial A.

Moreover, increasing the DFALIM and DFALIC parameters led to proportional increases in the predicted mean crush force. As expected, DFALIC (fibre compressive failure strain) was the most influential parameter in increasing the mean crush force value because, under axial compressive loading, the 0° and ±45° plies experienced compression along the fibre direction. Thus, increases in DFALIC directly increased the loading capacity of these elements and resulted in a component with higher energy-absorbing capacity. However, excessively high values of DFALIC yielded an

unreasonable material response not representative of the brittle behaviour of CFRPs because significant deformation of the element could take place before deletion. Additionally, it was of interest to keep the failure strains as close to the experimental values as possible. Trial and error resulted in the parameters summarized in Table 6.4. These parameters maximized mean crush force without predicting unreasonable deformation of the channel. Nonetheless, this maximized mean crush force continued to underestimate the performance of the channel in comparison to the experimental average (Figure 6.11). Thus, it was necessary to also consider the nonphysical parameters, trigger thickness and additional changes to the DFAILx parameters if necessary.

Table 6.4: Values of DFAILx selected for MAT_054 to maximize the mean crush force while minimizing non-physical deformation of the channel in the simulation model of the baseline condition.

Parameter	Value (mm/mm)
DFAILM	0.242
DFAILS	0.1
DFAILT	0.066
DFAILC	-0.5
EFS	0
SOFT	0.60

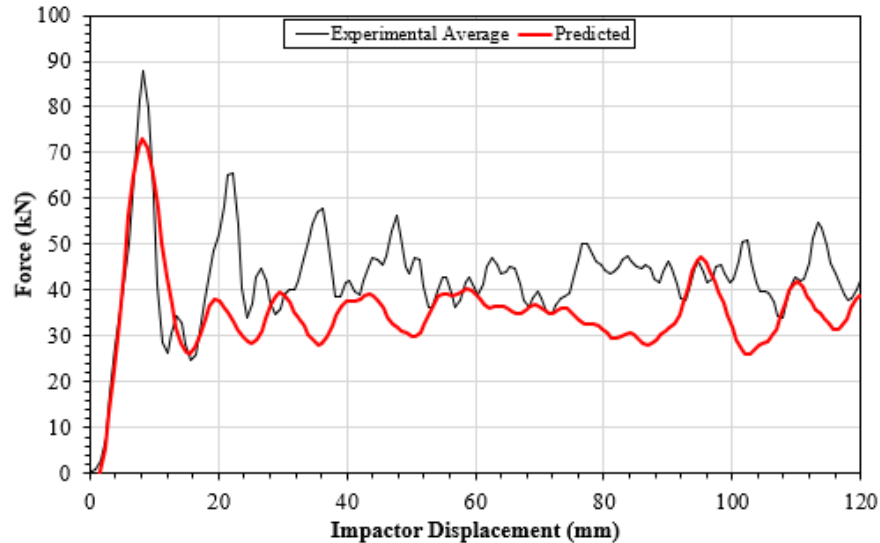


Figure 6.11: Predicted and experimental [76] force-displacement responses of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using the modified (Table 6.4) DFAILx parameters.

6.1.3.2 Influence of ALPH

For values of ALPH greater than 0, there was minimal difference in the mean crush force and total energy absorption observed (Figure 6.12). Thus, the initial ALPH value of 0.1 was maintained for subsequent simulations.

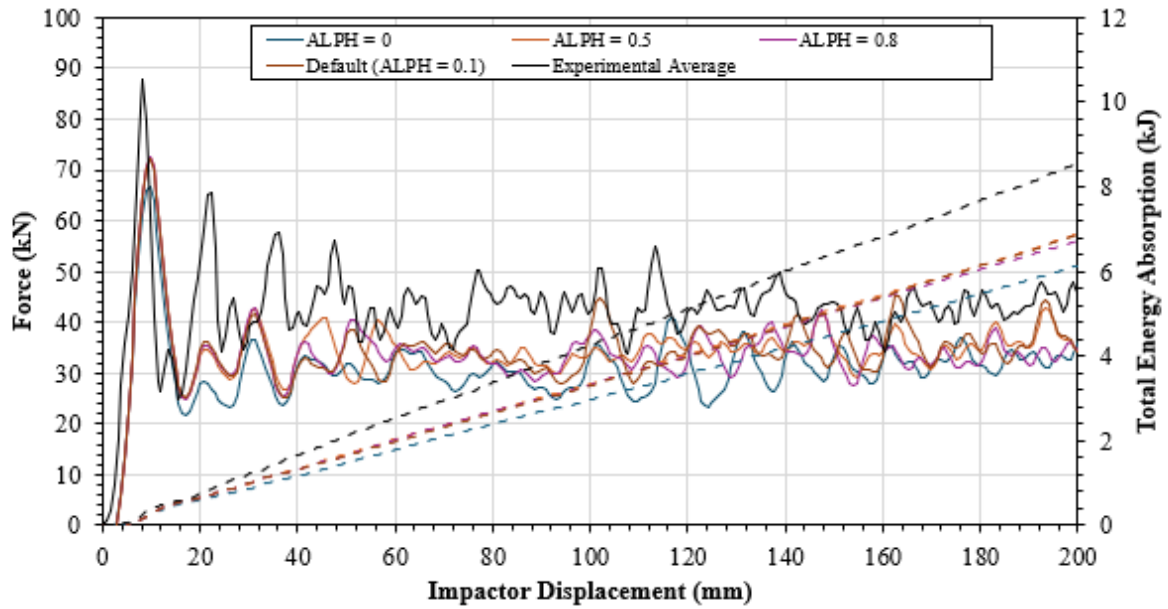


Figure 6.12: Influence of ALPH on the predicted force-displacement response of the simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.3 Influence of FBRT

For very low values of FBRT, an unstable force-displacement response was predicted. Values of FBRT that are too low soften the fibre tensile strength after matrix compressive failure such that the channel will fail as a result of the tension induced by bending instead of axial compression. Since the $\pm 45^\circ$ and 90° plies tended to fail under matrix compression before fibre failure, they were most influenced by this parameter. Any value of longitudinal tensile strength greater than the maximum tensile stress as a result of the bending moment had little influence on the results. Hence, an FBRT = 0.5 presented very similar results to an FBRT = 0.7. A slight increase in total energy absorption was achieved at a value of 0.5 as well as a more stable response (Figure 6.15). Thus, an FBRT value of 0.5 was maintained for subsequent simulations.

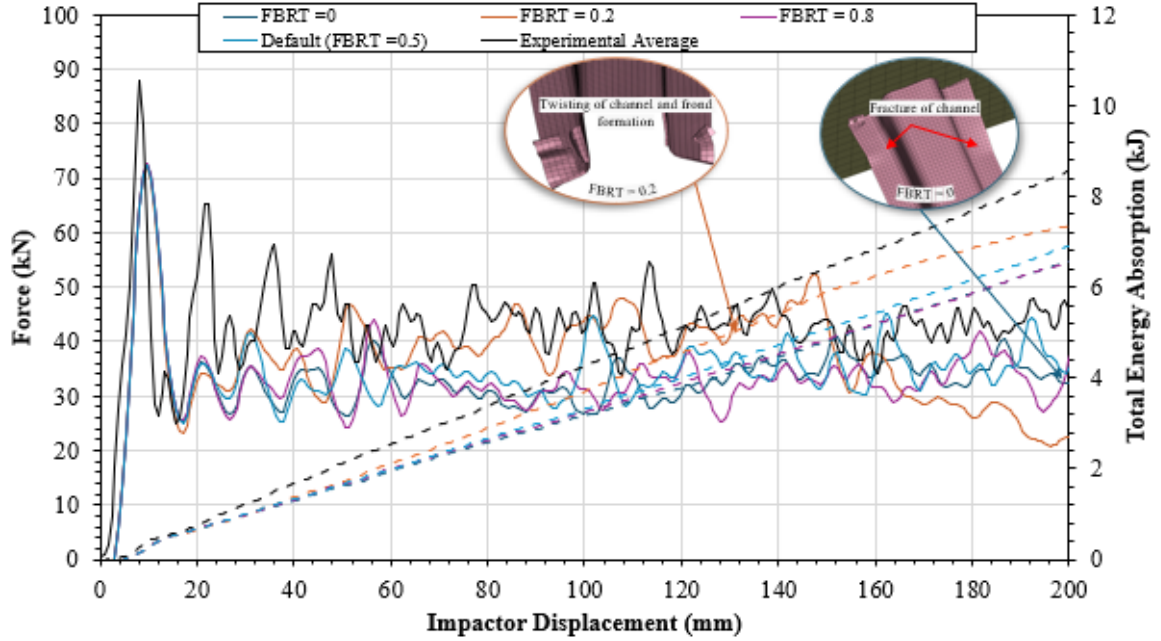


Figure 6.13: Influence of FBRT on the predicted force-displacement response of the simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.4 Influence of YCFAC

For high values of YCFAC, the loading behaviour became unstable, while for low values of YCFAC, the total energy absorption was reduced. YCFAC scales longitudinal compressive strength after compressive matrix failure, and thus high values of YCFAC result in higher mean crush force (Figure 6.14). Since the 0° plies experienced higher compressive stresses than the other plies, they often failed in the compressive fibre mode before compressive matrix failure was reached. Thus, this parameter mostly affected the $\pm 45^\circ$ and 90° , as they were capable of reaching the compressive matrix failure mode before reaching the fibre compressive failure mode. For the case when $YCFAC = 4.0$, the simulation model predicted less element deletion and increased deformation in the channel which corresponded to a drop in force with each deformity. In contrast, lower values of YCFAC (i.e., 1.0) resulted in predictions with increased element failure but an underprediction of the channel performance. Thus, the initial value of 2.0 was maintained.

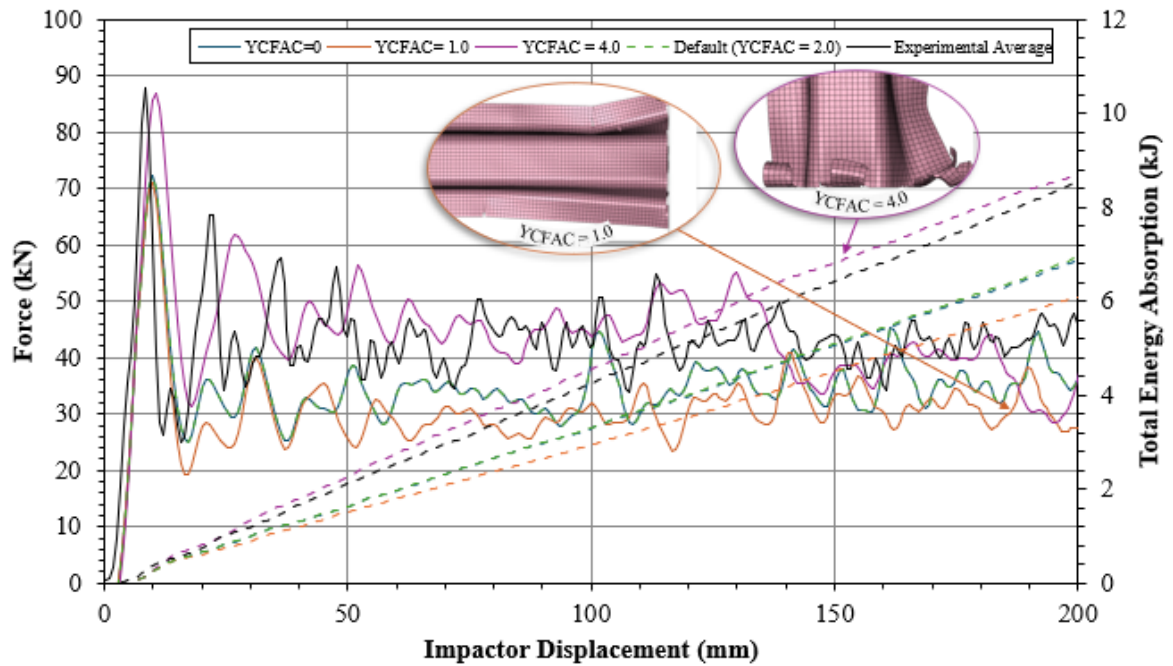


Figure 6.14: Influence of YCFAC on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.5 Influence of BETA

Minimal changes in the mean crush force and total energy absorption were observed for changes in the value of BETA (Figure 6.15). This may have been because BETA influences the Chang-Chang longitudinal tensile stress failure criterion. However, element deletion was not defined to be controlled by failure stress. Thus, the default value of 0.5 was maintained.

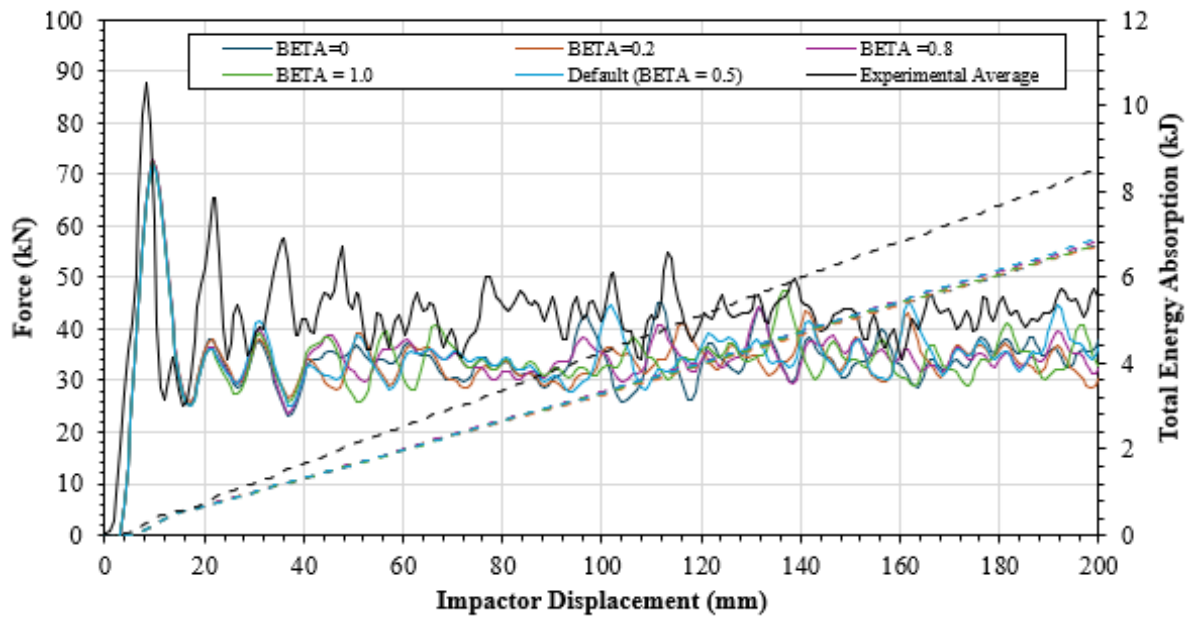


Figure 6.15: Influence of BETA on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.6 Influence of PEL

Minimal changes in the total energy absorption and mean crush force were observed with changes in PEL (Figure 6.16). This was likely because the speed of the impact was too fast for a difference in the number of integration points that must fail before element deletion to have been impactful. More specifically, if one integration point failed under progressive crushing, at a dynamic loading rate, it was soon followed by the failure of all other integration points. It is more likely that at a quasi-static rate, whereby there could be relatively substantial time between the failure of each integration point, the influence of PEL would be more pronounced. Thus, a PEL value of 100% was selected given the slight increase in energy absorption.

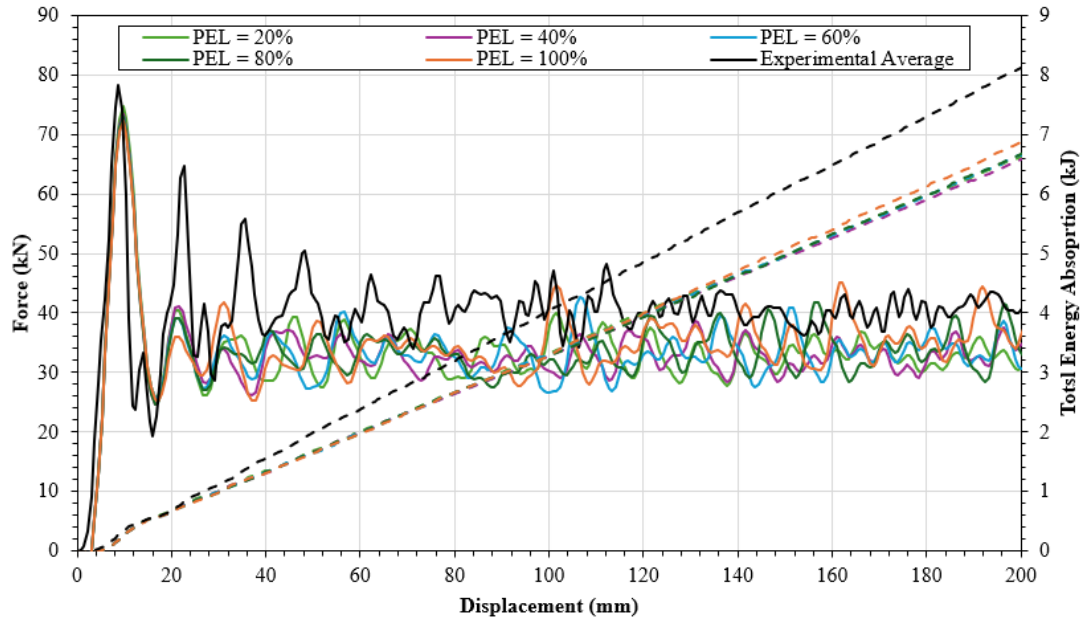
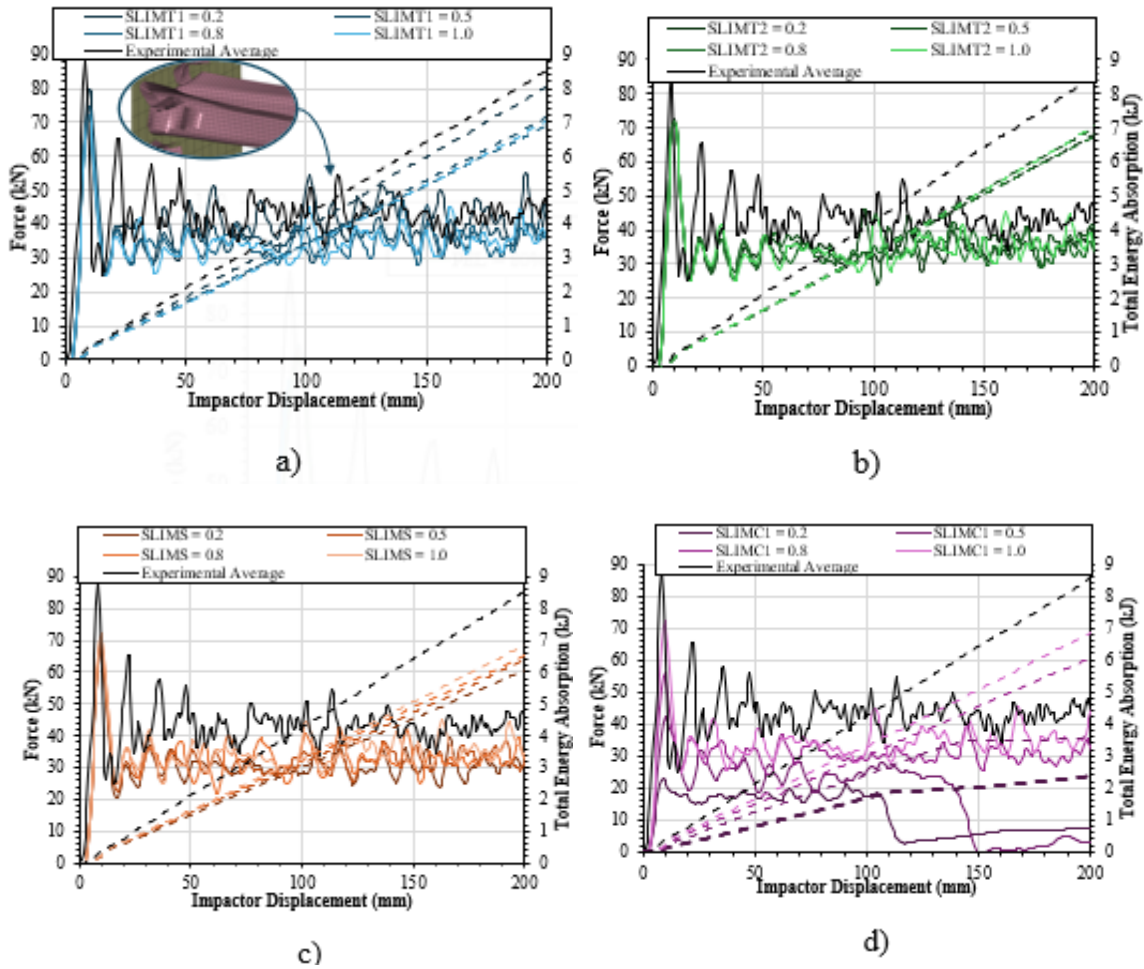


Figure 6.16: Influence of PEL on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.7 Influence of the SLIM Parameters

Next, the influence of the SLIM parameters (SLIMT1, SLIMT2, SLIMC1, SLIMC2, and SLIMS) on the mean crush force were considered (Figure 6.17). Minimal changes were observed with changes in SLIMT1 except at lower values (Figure 6.17(a)). For a SLIMT1 value of 0.2, the mean crush force was not only over-predicted, but a less stable force-displacement response was observed. Thus, the initial value of 1.0 for SLIMT1 was selected for subsequent simulations. Next, minimal changes in the mean crush behaviour and total energy absorption were observed for changes in SLIMT2 (Figure 6.17(b)). Proportional increases in the mean crush force and total energy absorption were observed for increases in SLIMS (Figure 6.17(c)). Thus, a value of 1.0 was chosen to maximize the mean crush force. Significant changes in the force-displacement behaviour were observed for changes in the SLIMC1 value, with stable behaviour and total energy absorption being maximized for a value of 1.0 (Figure 6.17(d)). Lastly, a proportional increase in the mean crush force and total energy absorption was also observed for increasing values of SLIMC2 (Figure 6.17(e)). Thus, a SLIMC2 value of 1.0 was selected for subsequent simulations.

The use of SLIM parameters introduced “plastic-like” behaviour [157] and prevented premature element deletion. As a result, higher values for the SLIMC1 and SLIMS parameters increased the mean crush force (and thus energy absorption) because fewer elements were deleted. For the axial loading condition considered, longitudinal compressive stresses in the 0° plies and shear stresses in the $\pm 45^\circ$ plies were most significant, which is the reason that the predicted results were most sensitive to the SLIMC1 and SLIMS parameters, respectively. For example, when SLIMC1 was set to 0.2 or 0.5, it was observed that elements failed and were deleted near the fixture constraint which resulted in unstable force-displacement responses (Figure 6.17(d)).



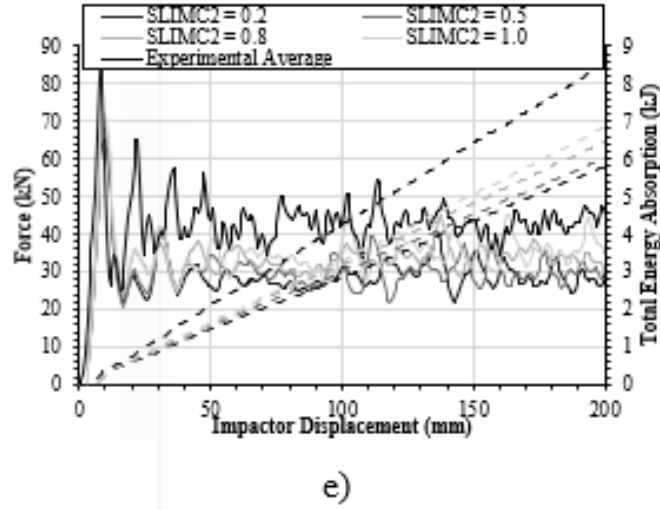


Figure 6.17: Influence of varying (a) SLIMT1, (b) SLIMT2, (c) SLIMS, (d) SLIMC1, and (e) SLIMC2 parameters on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.8 Influence of SOFT

Consistent with previous parametric studies (i.e. Section 6.1.1, (Dharmaraj, 2020)), increases in the SOFT value were found to proportionally increase the mean crush force and total energy absorption of the baseline simulation model. With the above modelling decisions included, a SOFT value of 0.8 resulted in a force-displacement response with a mean crush force exceeding 40 kN while a SOFT value of 0.60 resulted in mean crush forces below this (Figure 6.18). Thus, a SOFT value of 0.7 was selected for subsequent simulations.

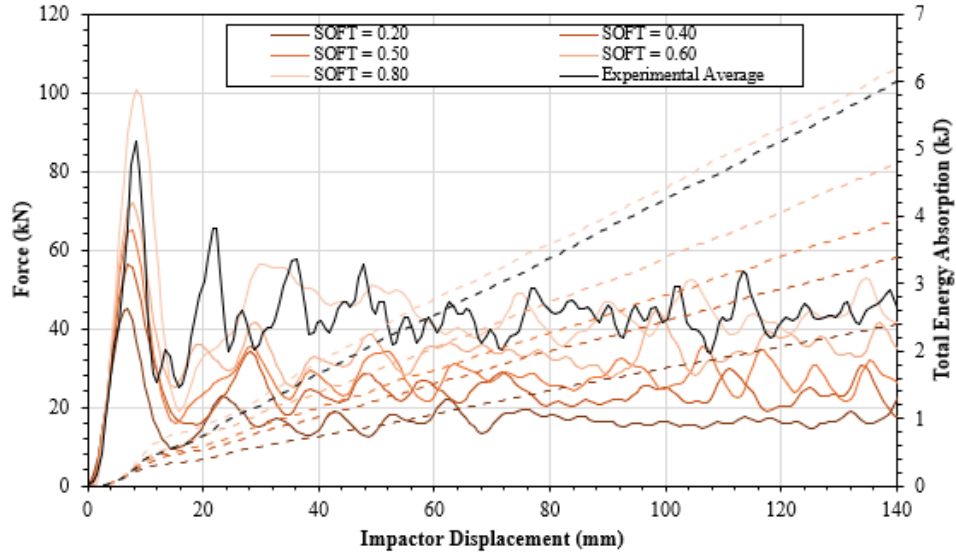


Figure 6.18: Influence of SOFT on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.9 Influence of Trigger

Lastly, the thickness of trigger elements of the channel was adjusted to better capture the experimental peak force after filtering with a 300 Hz SAE filter. Per the suggestions of Feraboli and Wade [39] on the use of the trigger as a softening factor for the first row of elements, selecting a SOFT value of 0.7 would suggest that the thickness of the trigger should be about 70% of the thickness of the rest of the channel; 0.24 mm per ply for this study. While the peak force almost consistently increases with increasing trigger thickness, minimal changes in the energy absorption can be observed (Figure 6.19). Thus, unless initiating a catastrophic or premature failure, the peak force contributes minimally to the amount of energy absorbed during progressive crushing and the final value is significantly influenced when filtering is applied (as was the case with this numerical methodology).

For the parameters selected, it was observed that with increasing trigger thickness, the behaviour of the channel was less physically representative and skewed towards local buckling as a failure mode. This is reflective of the behaviour observed during experiments when a trigger is not

machined into a channel. Given that the experimental average peak force was 82.46 kN, a trigger thickness of 0.125 mm per layer was selected for subsequent simulations.

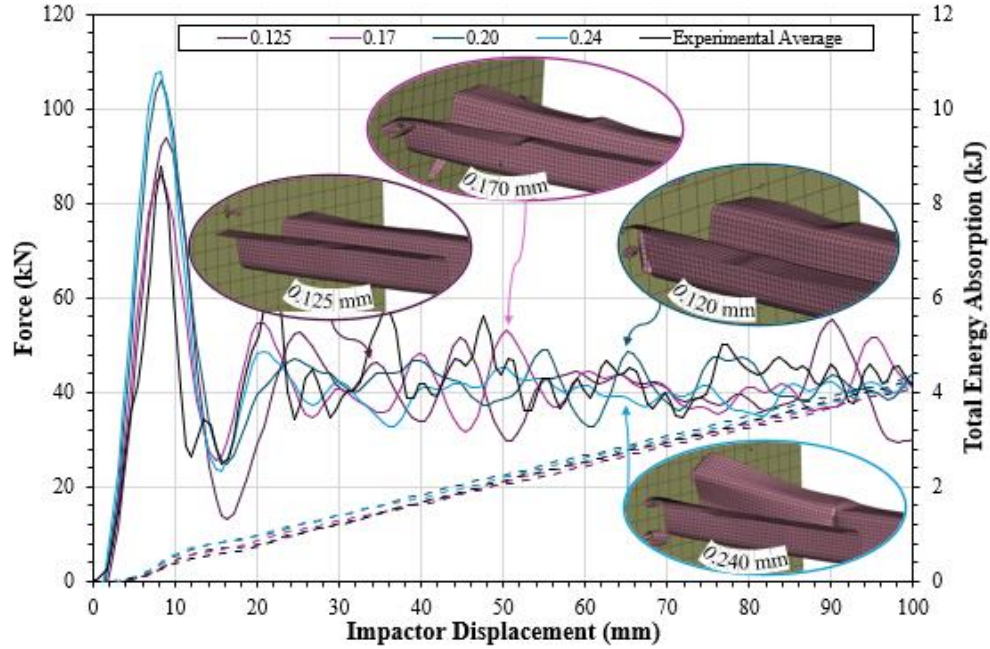


Figure 6.19: Influence of trigger thickness on the predicted force-displacement response of simulation model of the baseline condition with a structured quadrilateral mesh applied. Average experimental data is taken from [76].

6.1.3.10 Final Values for Calibration

The final calibrated non-physical MAT_54 material model parameters are presented in Table 6.5. These calibrated parameters along with the experimentally derived mechanical properties (Table 3.1) were used to simulate quasi-static and dynamic axial crush and three-point bending experiments for channels with different geometries and stacking sequences (see Sections 6.2-6.7). It should be noted that a structured quadrilateral mesh was used for all cases simulated hereafter. The predicted results were compared with available experimental results from the literature [25], [76] or with the experimental results presented in Chapter 5, where experimental averages were used for all comparisons.

Table 6.5: Final calibrated non-physical parameters selected for MAT_054 using the simulation model of the dynamic axial loading of a [0/45/90]_s hat channel (i.e., baseline condition) and a structured quadrilateral mesh to represent the hat channel parts.

Parameter	Value
RO, E _A , E _B , PRBA, G _{AB} , G _{BC} , G _{CA}	Experimentally derived material properties (Table 3.1)
DFAILM	0.242
DFAILS	0.100
DFAILT	0.100
DFAILC	-0.55
EFS	0
TFAIL	2.5E-07
ALPH	0.1
SOFT	0.70
FBRT	0.5
YCFAC	2.0
XC, XT, YC, YT, SC	Experimentally derived material properties (Table 3.1)
BETA	0.5
PEL	100
SLIMT1, SLIMT2, SLIMC1, SLIMC2, SLIMS	1.0
Trigger Thickness	0.125

6.2 Open Channel Dynamic Axial Loading Simulation Model Results

The predicted results from simulations of open channels with different geometries and laminate stacking sequences subject to dynamic axial loading are presented in this section. The final calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. The predicted results were filtered at 300 Hz SAE to minimize the oscillations introduced by the structured quadrilateral mesh.

6.2.1 $[0/\pm 45/90]_s$ Channels

6.2.1.1 Hat Channel

For the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel (i.e., simulation of baseline condition), there was a good agreement between the experimental average and predicted force-displacement response (Figure 6.20). The simulation model accurately captured the initial slope of the force-displacement response at the onset of loading and the subsequent progressive crushing of the channel. The failure mode observed during experiments for this stacking sequence and geometry was fragmentation [25], which was well predicted. Most elements were deleted during the simulation, and the channel failed in a brittle manner. Differences between the predicted and experimental average peak force, mean crush force, and energy absorption over the first 200 mm of impactor displacement were +13.46%, -0.26%, and -0.12%, respectively (Figure 6.21).

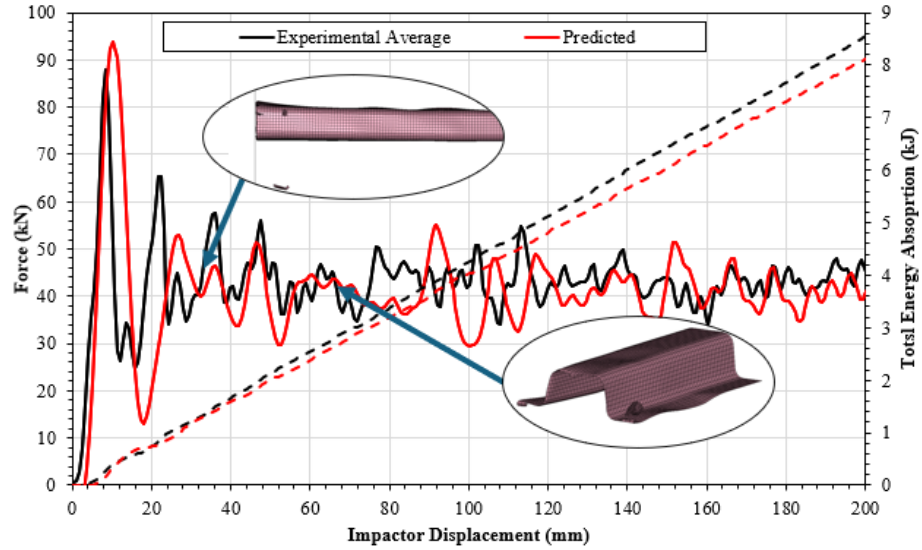


Figure 6.20: Predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel using a calibrated MAT_054. Average experimental force-displacement response in black [76].

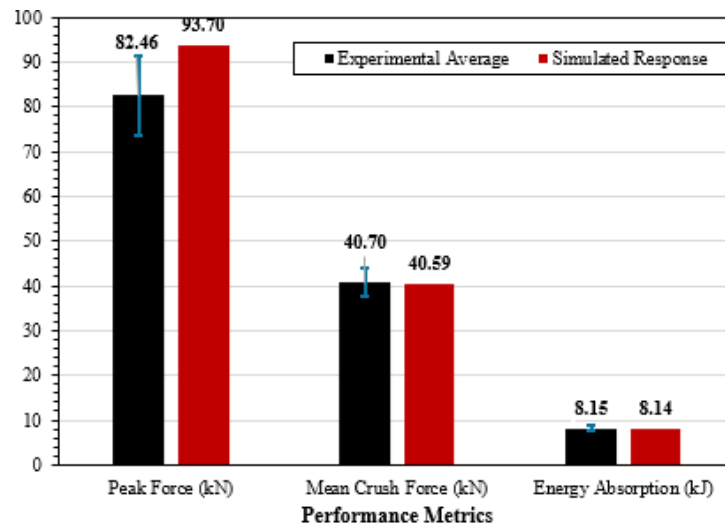


Figure 6.21: Comparison of the predicted and average experimental [76] performance metrics of the $[0/\pm 45/90]_s$ hat channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 200 mm.

6.2.1.2 Corrugated Channel

For the dynamic axial loading of the $[0/\pm 45/90]_s$ corrugated channel, good agreement between the experimental average and predicted force-displacement response was observed with the exception of the peak force (Figure 6.22). This was a result of the filter applied, which significantly altered the peak force initially predicted by the model. The simulation model also predicted pure fragmentation under progressive loading, which aligned well with the average experimental performance [25]. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 160 mm of impactor displacement were -30.3%, 0.24%, and 0.08%, respectively (Figure 6.23). The displacement simulated was based on the available experimental data, which also loaded the channel until 160 mm of impactor displacement.

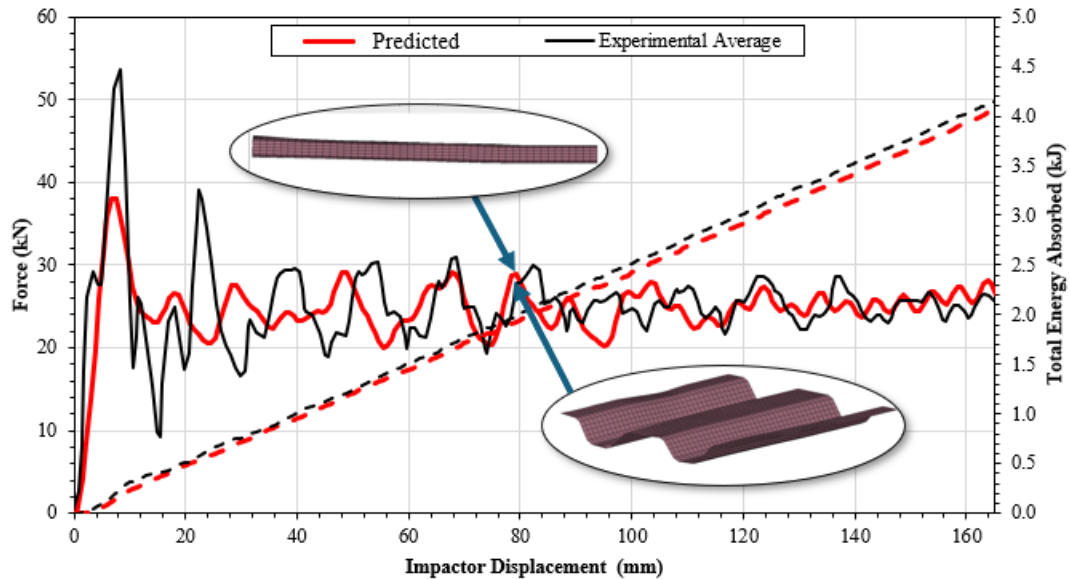


Figure 6.22: Predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ corrugated channel using MAT_054 with calibrated values and average experimental force-displacement response [25].

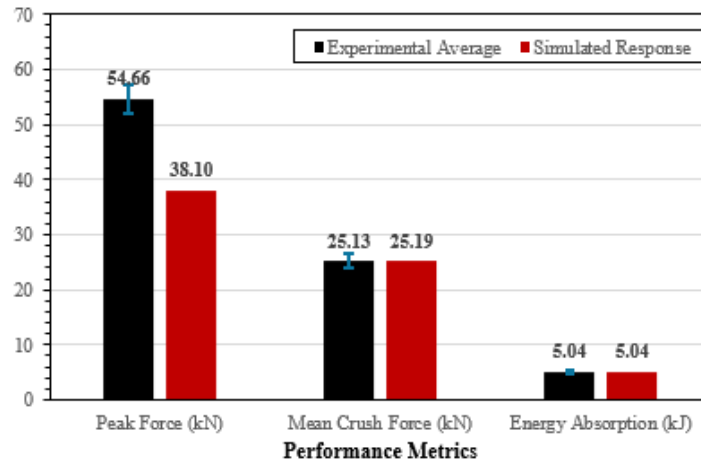


Figure 6.23: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm 45/90]_s$ corrugated channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 160 mm.

6.2.1.3 Tapered Channel

For the dynamic axial loading of the $[0/\pm 45/90]_s$ tapered channel, good agreement between the experimental average and predicted force-displacement response was observed. The force-displacement response was overpredicted, but the shape was well approximated. Additionally, some non-physical behaviour was predicted. More specifically, the folding of the channel inwards and the bending of the flanges (Figure 6.24) were not behaviours observed experimentally. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 200 mm of impactor displacement (i.e., the straight portion of the channel) were -3.0%, +14.7%, and +14.7%, respectively (Figure 6.25).

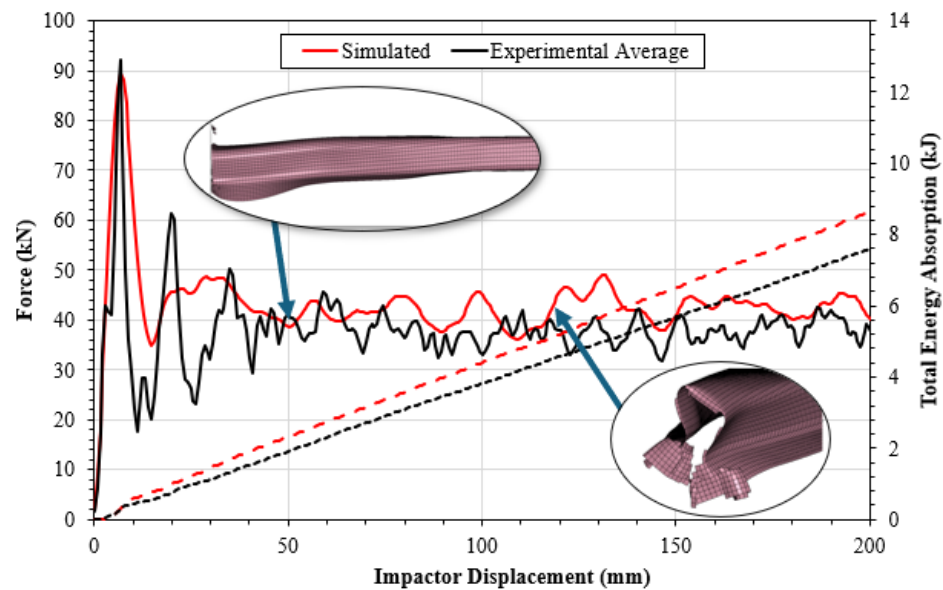


Figure 6.24: Predicted force-displacement response of the dynamic axial loading of the $[0/\pm 45/90]_s$ tapered channel using MAT_054 with calibrated values and average experimental force-displacement response.

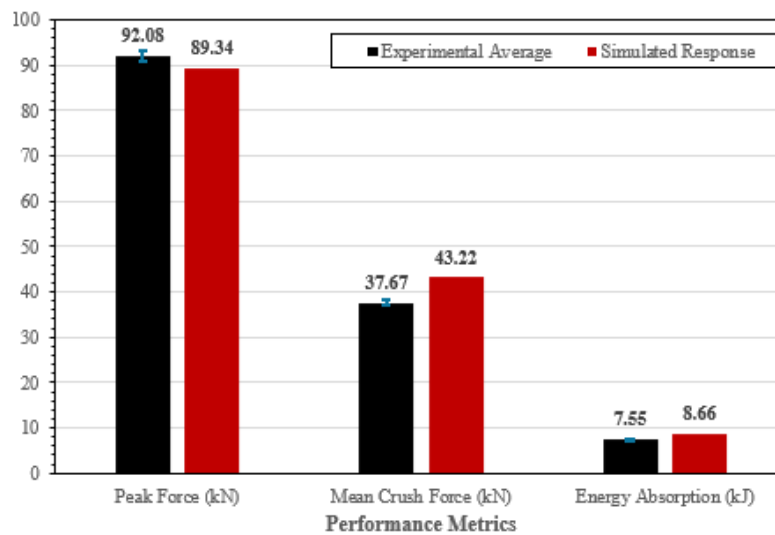


Figure 6.25: Comparison of the predicted and average experimental performance metrics of the $[0/\pm 45/90]_s$ tapered channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 200 mm.

6.2.1.4 Comparison of $[0/\pm 45/90]_s$ Channels Predicted Results

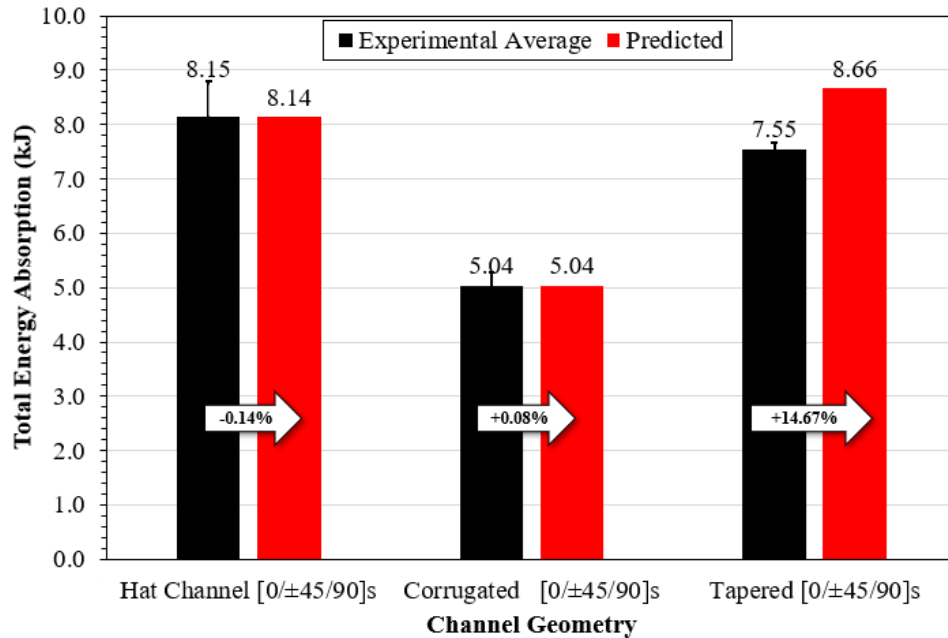


Figure 6.26: Comparison of the experimental and predicted total energy absorption of $[0/\pm 45/90]_s$ channels under dynamic axial loading. The percentages indicate the deviation between the predicted and average experimental values. The experimental results for the hat and corrugated channels were taken from [76] and [25].

Beginning with the dynamic axial loading of the $[0/\pm 45/90]_s$ hat, corrugated, and tapered channels, the discrepancies between the predicted and experimental average total energy absorption were 0.12%, +0.08%, and +14.67%, respectively (Figure 6.26). The minor discrepancy for the hat channel prediction is expected given that the non-physical parameters of MAT_054 were calibrated for this simulation model (i.e., baseline condition). The deviations were also reasonable for the corrugated and tapered hat channel predictions. The respective simulation models reasonably captured the physical deformation observed experimentally.

For the tapered channel, the total energy absorption was overpredicted by the simulation model due to the non-representative deformation behaviour of the channel flanges and walls (Figure 6.24). Since less fragmentation than observed in the experiments was predicted (Section 5.1.1), the

material fronds, specifically at the flanges, remained in contact with the platen during progressive crushing which led to increased friction and, thus, energy dissipation. Less fragmentation was predicted due to the artificially high longitudinal compressive failure strain (-55%) used in MAT_054. The effect of overestimating DFAILC may be most pronounced for the tapered hat channel because it was 200 mm longer than the other channels (i.e., less stable) and had a change in the cross-sectional geometry. Thus, the channel experienced more significant bending moments which resulted in local deformation as opposed to axial crushing. Additionally, it can be recalled that the SEA of the tested tapered hat channel under dynamic axial loading was notably less when compared to the straight hat channel (Figure 5.13). This was due to observed process-induced local defects (i.e., fibre misalignment) in the tapered region of the channel (Section 5.1). It is probable that the fibres were also misaligned within the straight portion of the tapered hat channel which was not accounted for within the simulation model. This may have further contributed to the overpredicted channel performance. To demonstrate the effect of the folding behaviour/remaining fronds on the total energy absorption, the simulation model for the tapered channel under dynamic axial loading was adjusted with a DFAILC value of 40% instead of 55%. The resultant predicted force-displacement response was better aligned with the experimental data (7.98 kJ) and the deviation of the predicted total energy absorption decreased to 5.74% (Figure 6.27). The updated simulation model predicted increased fragmentation. A comparison of the frictional energy (energy dissipated due to friction) for the original and adjusted simulation models shows a reduction in frictional energy for the latter where more fragmentation was observed (Figure 6.28).

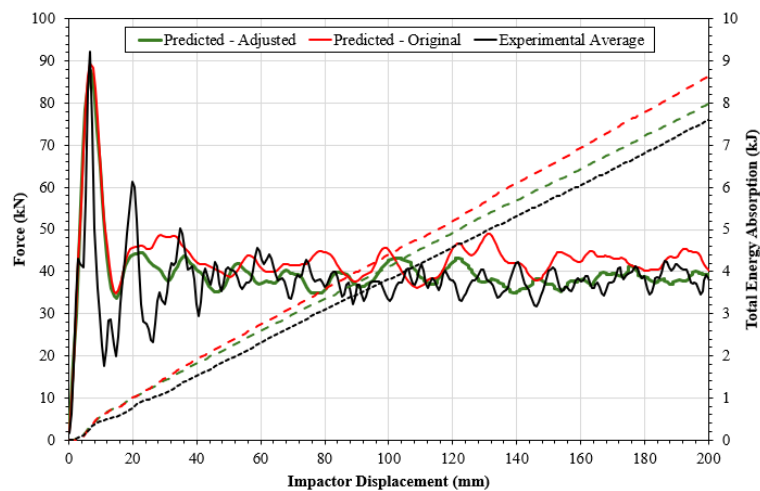


Figure 6.27: Comparison of the predicted and experimental force-displacement responses and energies absorbed for the dynamic axial loading of a $[0/\pm 45/90]_s$ tapered hat. “Predicted - Adjusted” refers to the simulation model with a DFAILC reduced to 40%.

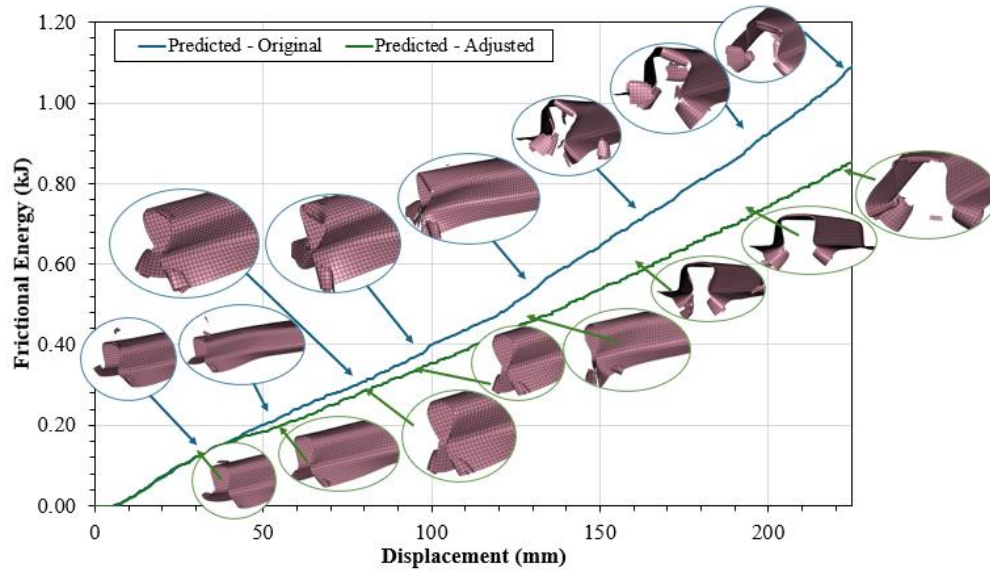


Figure 6.28: Comparison of the predicted frictional energies from the simulation models of a $[0/\pm 45/90]_s$ tapered channel subject to dynamic axial loading. “Predicted - Adjusted” refers to the simulation model with a DFAILC reduced to 40% while “Predicted - Original” refers to the original DFAILC of 55%.

6.2.2 $[\pm 45/0_2]_s$ Channels

6.2.2.1 Hat Channel

For the dynamic axial loading of the $[\pm 45/0_2]_s$ hat channel, the performance was underpredicted due to non-physical deformation predicted by the simulation. While brittle fracture and fragmentation were the predominant failure modes exhibited experimentally [25], over-pronounced folding was predicted by the simulation model which resulted in wider spans between the peak and troughs of the oscillations in the force-displacement response (Figure 6.29). This predicted behaviour was likely the result of the high failure strain under compression defined for MAT_054. Since the channel consisted of four 0° plies along the loading direction, there was more overall

deformation. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 200 mm of impactor displacement were +5.03%, -24.1%, and -23.7%, respectively (Figure 6.30).

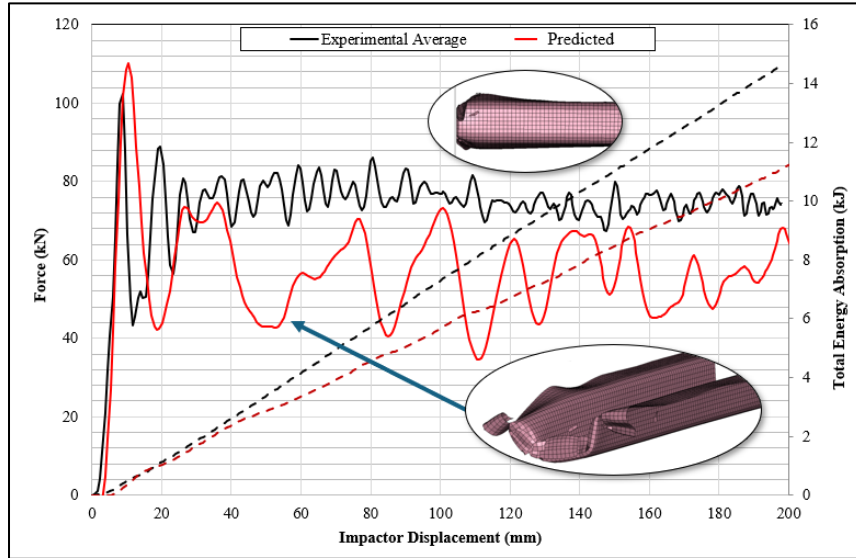


Figure 6.29: Predicted and experimental [76] force-displacement response of the dynamic axial loading of the $[\pm 45/0_2]_s$ hat channel using the calibrated MAT_054 parameters.

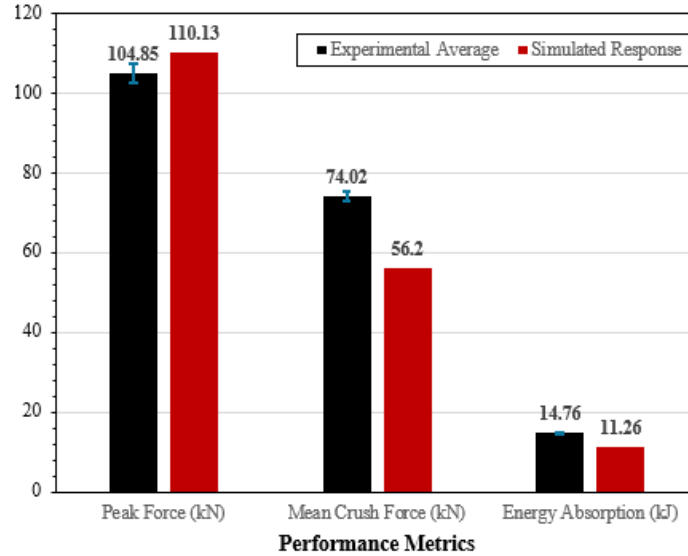


Figure 6.30: Comparison of the predicted and average experimental [25], [76] performance metrics of the $[\pm 45/0_2]_s$ hat channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 200 mm.

6.2.2.2 Corrugated Channel

For the dynamic axial loading of the $[\pm 45/0_2]_s$ corrugated channel, a premature and catastrophic failure was predicted. It was observed that until fracture occurs, there was good agreement between the predicted and experimental energy absorption (Figure 6.31). Additionally, until the failure, fragmentation was predicted which aligned with experimental observations [25]. However, as the impactor progressed in the simulation, the channel began to bend and twist under compressive loading which caused fracturing midway through the length of the channel. As a result, the simulation performance metrics were not calculated for this condition and the predicted result was considered unstable.

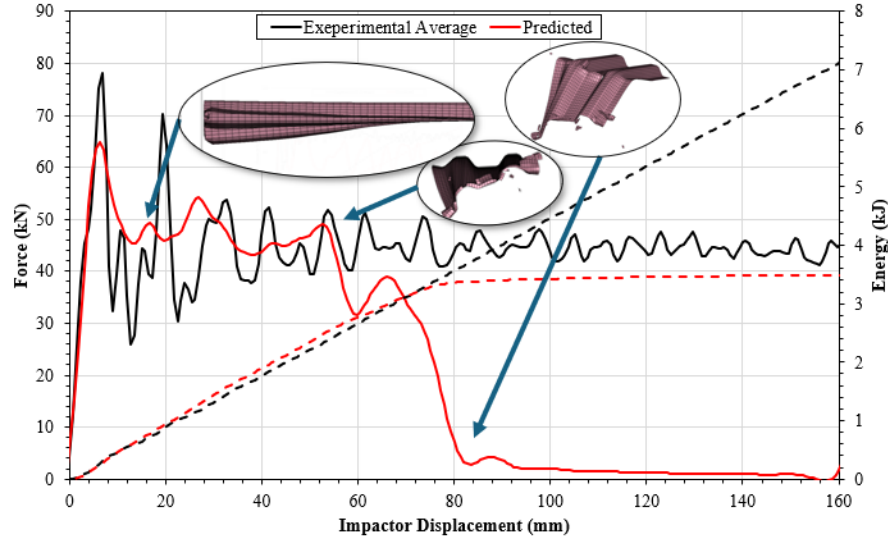


Figure 6.31: Predicted and average experimental [25] force-displacement response of the dynamic axial loading of the $[\pm 45/0_2]_s$ corrugated channel using the calibrated MAT_054 parameters.

6.2.2.3 Tapered Channel

For the dynamic axial loading of the $[\pm 45/0_2]_s$ tapered channel, a partially non-physical force-displacement response was predicted. The significant drop in the force-displacement response near 75 mm to 100 mm corresponded to the onset of permanent deformation in the channel and folding of the channel flanges and walls (Figure 6.32). The pronounced global bending of the channel and local bending of the flanges predicted were not consistent with the experiments. However, the formation of wide fronds can be considered similar to the formation of fronds and lamina bending observed experimentally (Figure 5.11). While the average crush force increased once progressive crushing of the channel resumed, the folding of the channel flanges and walls resulted in an overall unstable force-displacement response. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 200 mm of impactor displacement (i.e., straight portion of the channel) were +3.86%, -5.0%, and -5.2%, respectively (Figure 6.33).

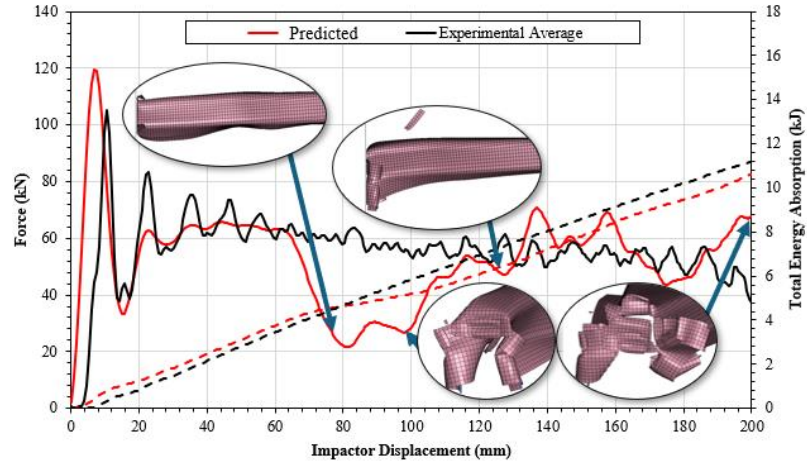


Figure 6.32: Predicted and average experimental force-displacement response of the dynamic axial loading of the $[\pm 45/0_2]_s$ tapered channel using the calibrated MAT_054 parameters.

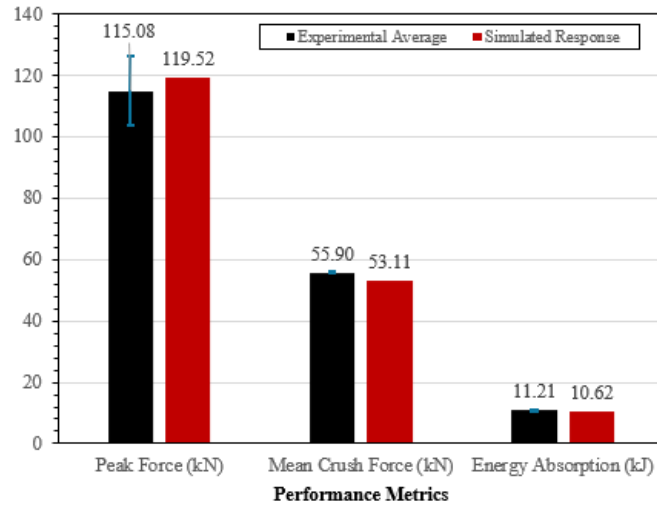


Figure 6.33: Comparison of the predicted and average experimental performance metrics of the $[\pm 45/0_2]_s$ tapered channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 200 mm.

6.2.2.4 Comparison of $[\pm 45/0_2]_s$ Channels Predicted Results

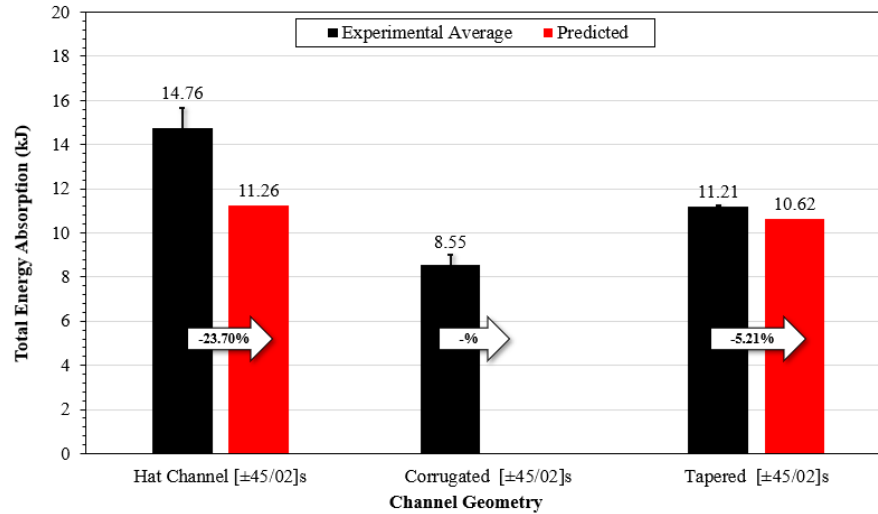


Figure 6.34: Comparison of the experimental and predicted total energy absorption of $[\pm 45/0_2]_s$ channels under dynamic axial loading. The percentages indicate the deviation between the predicted and experimental values. Note, the experimental results for the hat and corrugated channels were taken from [76] and [25].

For the dynamic axial loading of the $[\pm 45/0_2]_s$ open channels, the mean crush force was underpredicted and non-physical behaviour was observed for the hat and tapered channels, while premature fracture was predicted for the corrugated channel (Figure 6.29, Figure 6.31, and Figure 6.32). The deviations between the predicted and average experimental total energy absorption of the hat and tapered channels were -23.70% and -5.21%, respectively (Figure 6.34). Due to the premature failure of the corrugated channel, a total energy absorption was not calculated.

For the hat channel, the underprediction in total energy absorption can be attributed to the differences between the experimentally observed and predicted failure modes. Experimentally, Harvey et al. [25] reported fragmentation and brittle fracture along bent laminae (Figure 6.35). In contrast, significant folding/rolling of the flanges was predicted and crack propagation/element deletion was concentrated at the corners of the channel (Figure 6.35).

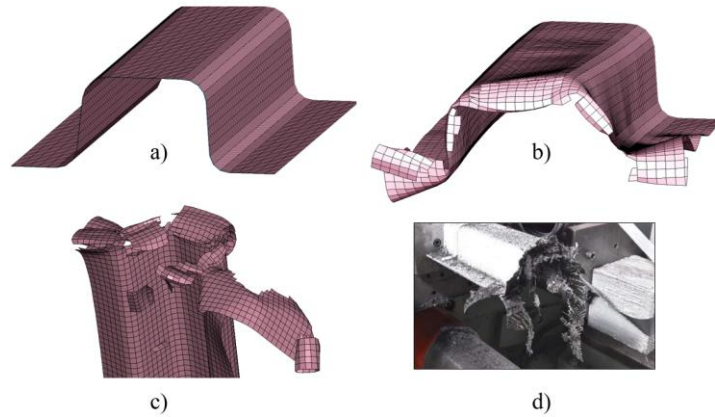


Figure 6.35: Simulation model for $[\pm 45/0_2]_s$ hat channel under dynamic axial loading: a) prior to loading, (b) at 100 mm of impactor displacement, and (c) at 200 mm of impactor displacement. (d) Post-testing image of $[\pm 45/0_2]_s$ hat channel under dynamic axial loading [25].

For the corrugated channel, there was good agreement between the predicted and experimental force-displacement responses until the channel fractured prematurely due to global buckling (Figure 6.31). Prior to buckling, a fragmentation of the component was accurately predicted (Figure 6.36a). However, the simulation model could not capture delamination, and the brittle fracture failure mode of the laminae was experimentally observed (Figure 6.36d). It is likely that the inability of the single-shell model to capture delamination led to premature fracture of the channel. It should be noted that this stacking sequence with a total of four 0° plies exhibited greater stiffness, and without being able to dissipate energy via ply separation, the channel catastrophically failed.

Lastly, for the tapered channel, there was a good agreement between the predicted and experimental total energy absorption. However, this correlation was coincidental and achieved despite the prediction of non-physical deformation. A good agreement between the experimental and predicted force-displacement response existed until approximately 65 mm of impactor displacement, after which significant local deformation of the channel was predicted (Figure 6.32). Numerically, the frond formation was as accurate a representation of the significant lamina bending experimentally observed as is possible with a single-layer model. As a result, there was only a small and reasonable difference in the final specific energy absorption. As aforementioned with the previous stacking sequence, this channel geometry – on a component level – has already been proven to require a lesser DFAILC for more accurate representation. Thus, it is unsurprising that the numerical prediction with the calibrated parameters predicted over-produced, non-physical folding.

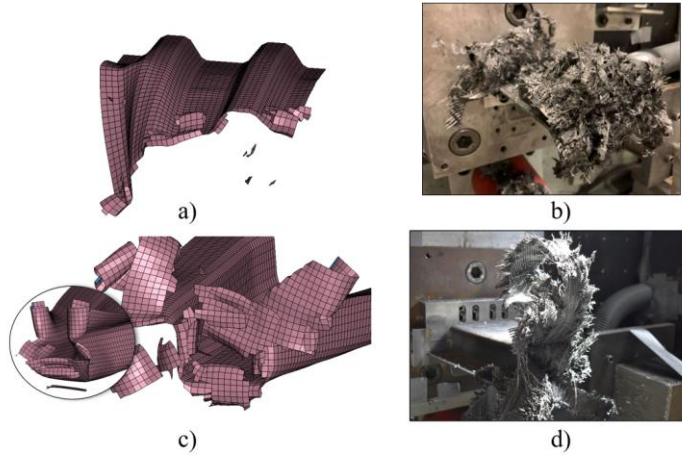


Figure 6.36: Dynamic axial loading of $[\pm 45/0_2]_s$ corrugated channel: a) Simulation model at 60 mm of impactor displacement, and (b) Post-test image [25]. Dynamic axial loading of $[\pm 45/0_2]_s$ tapered channel: (c) Simulation model at 250 mm of impactor displacement, and (d) Post-test image.

6.2.3 Discussion on Dynamic Axial Loading of Open channels

The simulation models utilizing the calibrated material model were able to accurately predict the performance of $[0/\pm 45/90]_s$ channels with three distinct geometries for the case of dynamic axial loading. In contrast, for $[\pm 45/0_2]_s$ channels with the same three geometries, the predicted performance and deformation were distinct from the experimental observations. The increased number of 0° plies would suggest that the $[\pm 45/0_2]_s$ channels should effectively be stiffer. However, because of the high failure strains defined (i.e. DFAILC), more plies aligned with the loading direction meant that more of the plies could further deform before the elements were deleted. As a result, increasingly non-physical behaviour was predicted. For the hat channel, the significant local deformations during loading resulted in a reduction of the mean crush force, and thus the overall energy absorption. Additionally, where delamination was a significant mechanism observed experimentally (i.e. the brittle fracturing of the corrugated channels [25] and splaying of the tapered channels), deviations in performance and behaviour were again predicted because the single shell model used to represent the parts was inherently incapable of capturing delamination. As a result, the corrugated channel fractured prematurely, and the tapered channel was predicted with overpronounced local deformation and bending of fronds.

Given the challenges observed for the $[\pm 45/0_2]_s$ channels, an effort was made to recalibrate the material model for the $[\pm 45/0_2]_s$ hat channel under dynamic axial loading to improve predicted results. Examples of the results and parameters explored are shown below (Figure 6.37). Despite over 50 trialled simulations, a combination of parameters which successfully predicted the mean crush force of the $[\pm 45/0_2]_s$ hat channel under dynamic axial loading without the prediction of significant non-physical behaviour was not found. While it was observed that increases in DFAILC did continue to lead to increases in mean crush force, it also introduced further local or global bending (and thus non-representative) behaviour into the predictions. Thus, to accurately capture the performance of channels with this stacking sequence under dynamic axial crushing, multiple layers of shell elements connected with cohesive elements can be modelled to capture delamination. Instead of modelling all layers, Mamalis et al. [72] modelled three plies – representing two groups of lamina bundles and an eventual debris wedge, to capture the effect of lamina bending and delamination without significantly increasing the computational requirements of a simulation model. This method could be worth exploring to improve predictions for the $[\pm 45/0_2]_s$ channels.

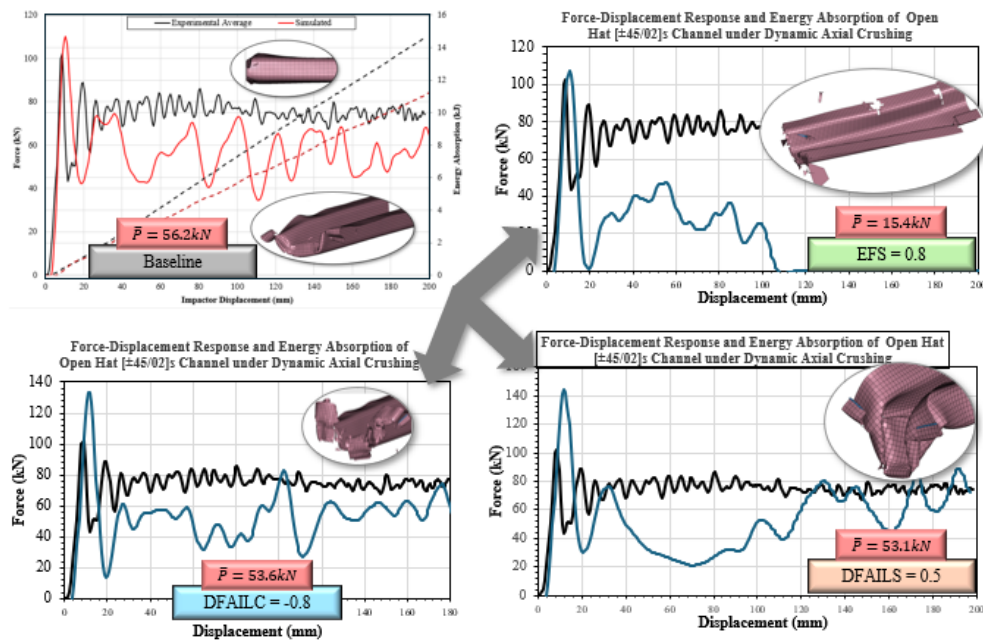


Figure 6.37: Examples of attempts to replicate the $[\pm 45/0_2]_s$ hat channel experimental results [25] under dynamic axial loading by tuning the indicated non-physical material model parameters.

6.3 Open Channel Quasi-Static Axial Loading Simulation Results

The force-displacement predictions of channels with different geometries and laminate stacking sequences subject to quasi-static axial loading are presented in this section. The final calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. The predicted results were filtered at 5 Hz SAE to minimize the oscillations introduced by the quadrilateral mesh. Additionally, the simulations were run to an impactor displacement of 90 mm to reduce computational time.

6.3.1 [0/±45/90]_s Channels

6.3.1.1 Hat Channel

For the quasi-static axial loading of the [0/±45/90]_s hat channel, the simulation model overpredicted the peak force and underpredicted the mean crush force and total energy absorption (Figure 6.38). Experimentally, fragmentation and brittle fracture were the predominant failure modes [25]. The overpredicted peak force corresponded to the buckling of the channel as a result of the initial impact. Rather than progressive crushing, a catastrophic failure would have been the result. However, due to the high value of DFAILC defined, the channel was permanently deformed in the middle of the length, and this deformation caused a significant reduction in the stress in the elements (and thus the crush force). Differences between the predicted and experimental average peak force, mean crush force, and energy absorption over the first 90 mm of impactor displacement were +85.2%, -33.4%, and -33.4%, respectively (Figure 6.39).

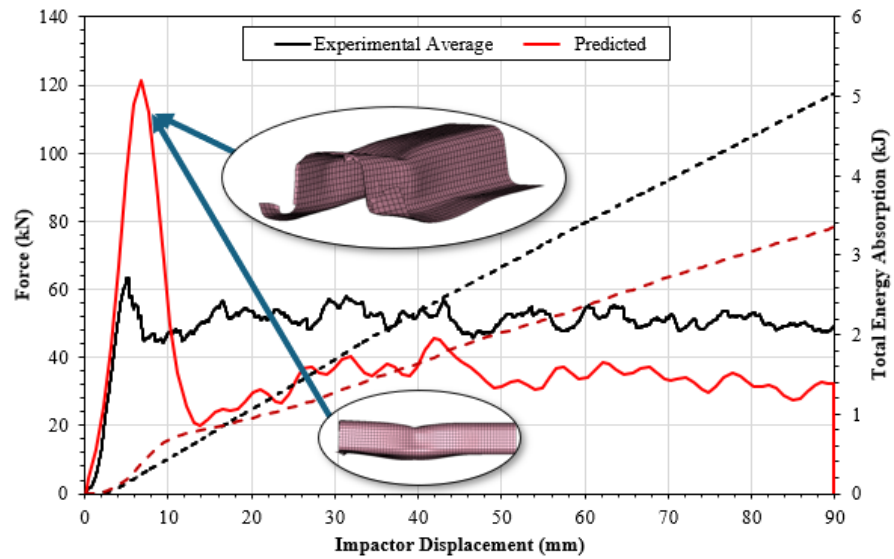


Figure 6.38: Predicted and average experimental [25] force-displacement response of the quasi-static axial loading of the $[0/\pm 45/90]_s$ hat channel using the calibrated MAT_054 parameters.

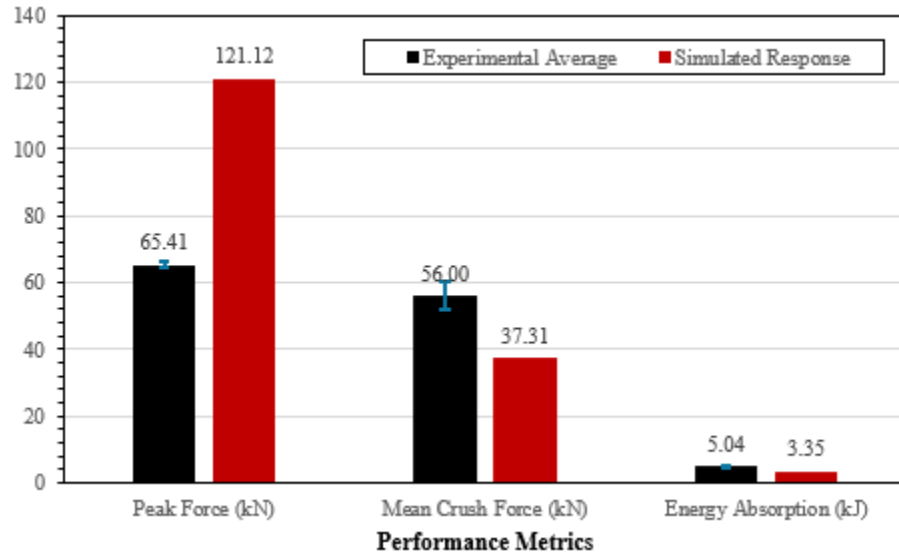


Figure 6.39: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm 45/90]_s$ hat channel under quasi-static axial loading. Performance metrics determined up to an impactor displacement of 90 mm.

6.3.1.2 Corrugated Channel

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ corrugated channel, the mean crush force and peak force were underpredicted (Figure 6.40). However, the overall shape of the force-displacement response was well replicated. A purely fragmentation failure mode was predicted whereas experimentally, brittle fracture was observed in conjunction with fragmentation. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 90 mm of impactor displacement were -34.3%, -39.3%, and -32.7%, respectively. (Figure 6.41).

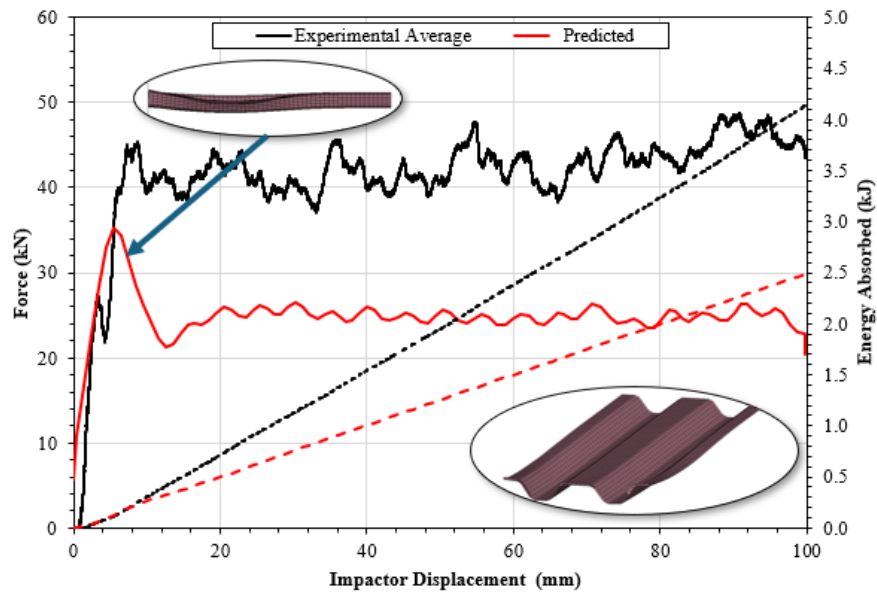


Figure 6.40: Predicted and average experimental [25] force-displacement response of the quasi-static axial loading of the $[0/\pm 45/90]_s$ corrugated channel using the calibrated MAT_054 parameters.

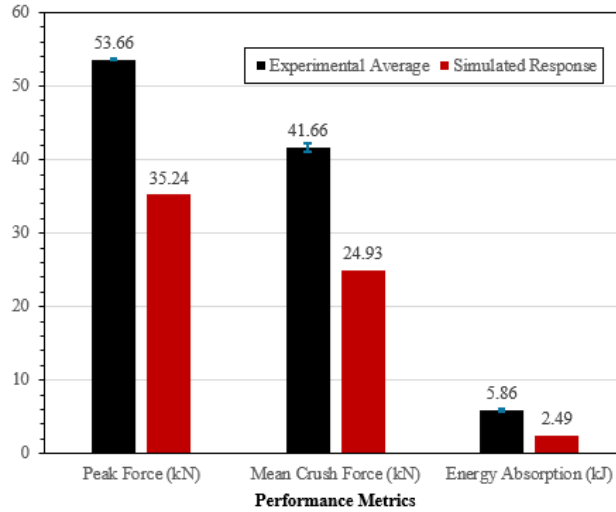


Figure 6.41: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm 45/90]_s$ corrugated channel under quasi-static axial loading. Performance metrics determined up to an impactor displacement of 90 mm.

6.3.1.3 Comparison of $[0/\pm 45/90]_s$ Channels Predicted Results

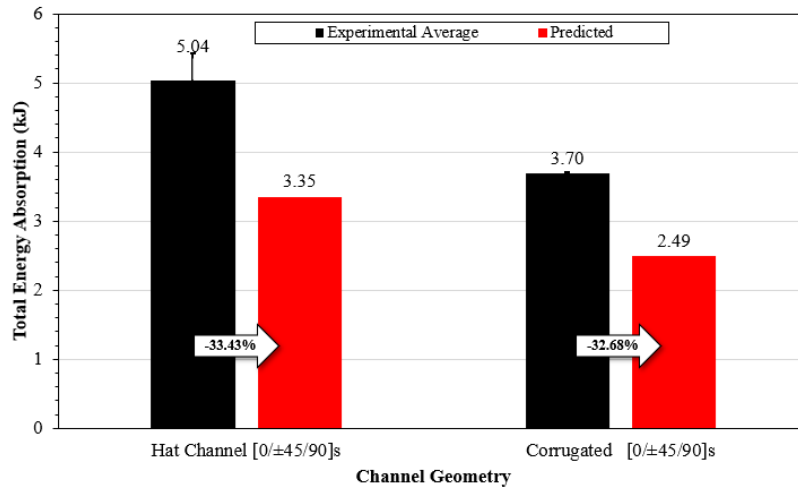


Figure 6.42: Comparison of the experimental [25] and predicted total energy absorption for the $[0/\pm 45/90]_s$ channels under quasi-static axial loading. The percentages indicate the deviation between the predicted and experimental values.

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ hat and corrugated channels, the discrepancy between the predicted and average experimental [25] total energy absorption was -

33.43% and -32.68%, respectively (Figure 6.42). The predominant failure mode observed through experiments [25] for both the hat and corrugated channels was brittle fracture (Figure 6.48), where lamina bending led to increased surface area in contact with the impactor. Since this phenomenon cannot be captured with the single-shell simulation model, the additional energy dissipation accompanied by these damage mechanisms was unaccounted for. Instead, fragmentation was the primary mode of failure predicted for both the hat and corrugated channels, similar to the dynamic simulations of the channels with the same stacking sequence. Additionally, the simulation models of the hat channel predicted global bending at the onset of loading which resulted in local permanent deformation (Figure 6.48a). The simulation model for the corrugated channel predicted significant bending in the flanges only and progressively crushed.

The inability of the simulation mode to accurately capture the failure mode can also be attributed to the fact that the strain-rate dependent material properties were not considered (a limitation with material model MAT_054). It was suggested by Farley [90] that when the damage mechanism is strain-rate dependent, so too would be the energy-absorbing capacity of the structure. Strain-rate-dependent behaviour was evident in the experimental results via the increased matrix cracking [76] and reduced fibre fracture observed under quasi-static testing in comparison to the dynamic tests.

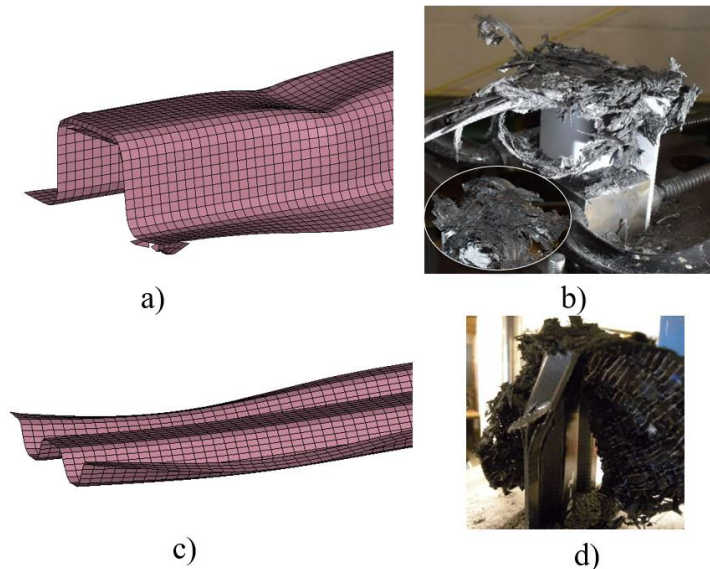


Figure 6.43: Quasi-static axial loading of $[0/\pm 45/90]_s$ hat channel: a) Simulation model at 9.4 mm of impactor displacement, and (b) Post-test image [25]. Quasi-static axial loading of

[0/±45/90]_s corrugated channel: (c) Simulation model at 5 mm of impactor displacement, and (d) Post-test image[25].

6.3.2 [±45/0₂]_s Channels

6.3.2.1 Hat Channel

For the quasi-static axial loading of the [±45/0₂]_s hat channel, the mean crush force and energy absorption were underpredicted, while the peak force was overpredicted. Additionally, the shape of the force-displacement response predicted by the simulation model included post-peak oscillations with greater amplitudes and wider spans than experimentally measured (Figure 6.44). This was indicative of less progressive crushing. The overpredicted peak force was likely the result of the change in stacking sequence. The [±45/0₂]_s hat channel is a stiffer component than the quasi-isotropic channel. As a result, catastrophic failure (i.e., buckling) at the onset of loading is more likely with an inadequate trigger. Thus, the overpredicted peak force suggests that the trigger was not well captured for this stacking sequence. To address this, changes to the trigger thickness or an alternative method for modelling the trigger which does not concentrate plies at the centre with half the thickness of the remainder of the channel would need to be considered. Under the initial peak load, the simulation model predicted local deformation in the centre instead of a catastrophic (brittle) fracture due to the high value of DFAILC defined. The local deformation resulted in a relaxation that led to a drop in the subsequent mean crush force below the experimental average. Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 90 mm of impactor displacement were +10.35%, -21.7%, and -21.4%, respectively (Figure 6.45).

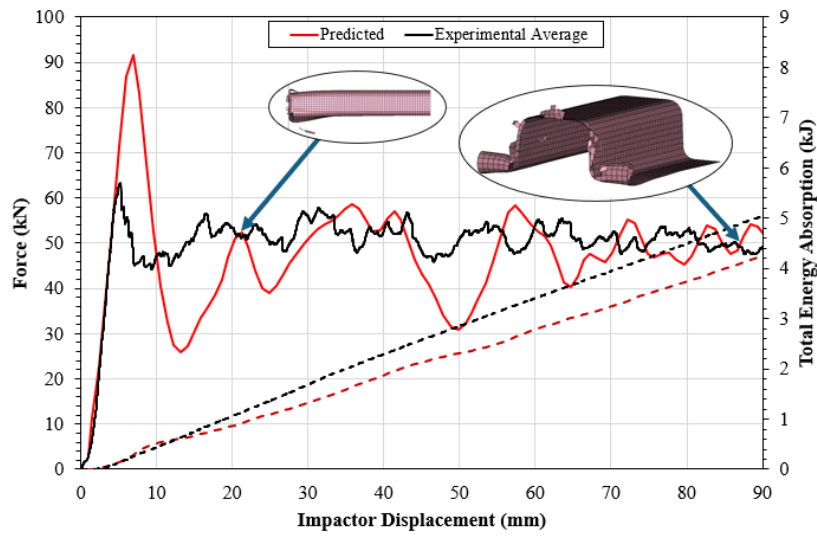


Figure 6.44: Predicted and experimental [25] force-displacement response of the quasi-static axial loading of the $[\pm 45/0_2]_s$ hat channel using the calibrated MAT_054 parameters

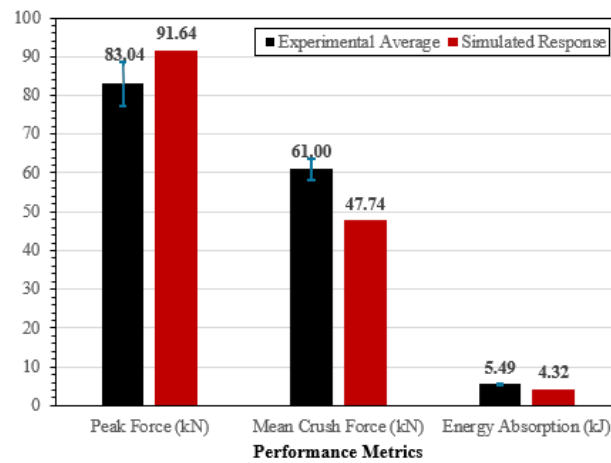


Figure 6.45: Comparison of the predicted and average experimental [25] performance metrics of the $[\pm 45/0_2]_s$ hat channel under quasi-static axial loading. Performance metrics determined up to an impactor displacement of 90 mm.

6.3.2.2 Corrugated Channel

Similar to the response under dynamic loading, premature fracture of the $[\pm 45/0_2]_s$ corrugated channel was predicted. Once again, until fracture occurred, there was relatively good agreement between the predicted and experimental energy absorption (Figure 6.46), with a slight

underprediction by the simulation model. Fragmentation was predicted by the simulation model, however, significant splaying was observed in the experiments [25]. Additionally, the simulation model again predicted twisting and global bending of the channel, causing fracture midway through the channel length. Failure as a result of bending suggests that the tensile failure strain of the component was not well captured during calibration. As a result, the simulation performance metrics are not calculated for this condition, and the predicted result was considered unstable.

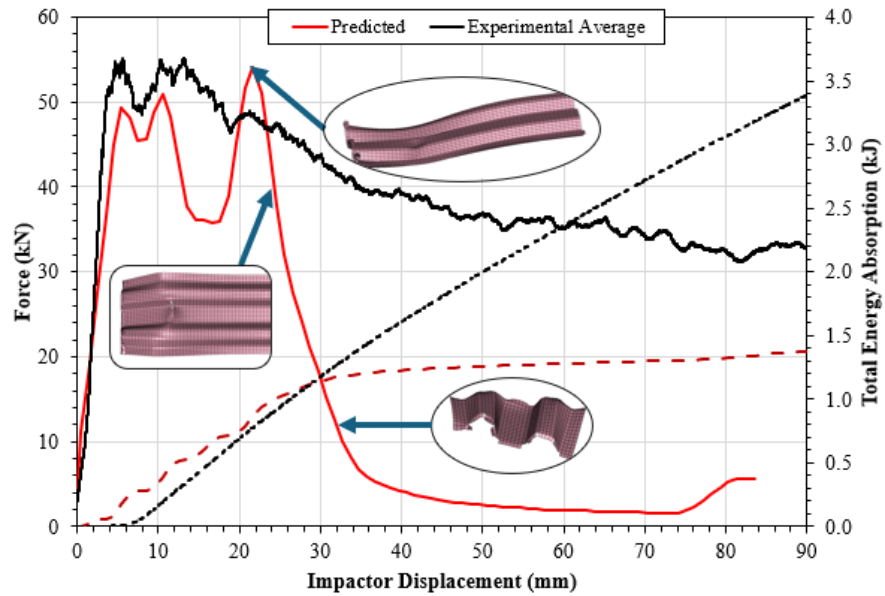


Figure 6.46: Predicted and average experimental [25] force-displacement response of the quasi-static axial loading of the $[\pm 45/0_2]_s$ corrugated channel using the MAT_054 calibrated parameters.

6.3.2.3 Comparison of $[\pm 45/0_2]_s$ Open Channel Predicted Results

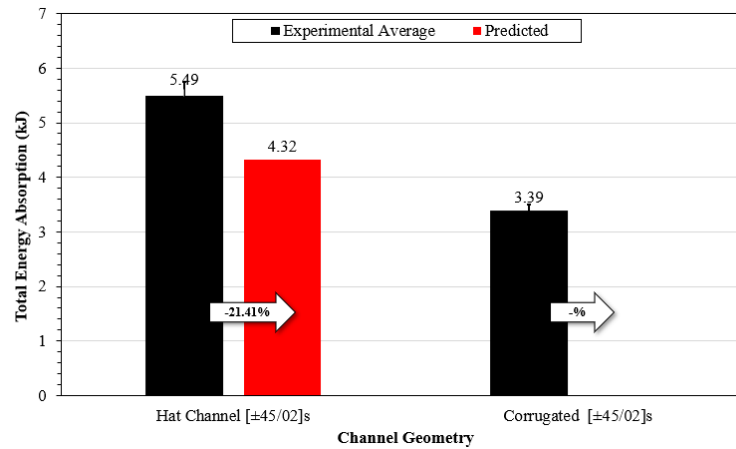


Figure 6.47: Comparison of the experimental [25] and predicted total energy absorption for the $[\pm 45/0_2]_s$ open channels under quasi-static axial loading.

Under quasi-static loading, the predominant failure mode observed through experiments for the hat and corrugated channels was extensive splaying [25]. Delamination between the 0° plies was observed along with splitting cracks between the 0° ply fibre bundles (Figure 6.48). The limited capability of the single-shell simulation model to capture delamination and splaying resulted in an underprediction of the total energy absorbed for the hat channel and premature fracture of the corrugated channel. A limited degree of lamina bending was visible in the simulation of the hat channel (Figure 6.48a). Again, the inability of the simulation mode to accurately capture the failure mode may in part be due to the fact that the material model (MAT_54) did not account for the strain rate-dependent material properties. Splaying is characterized by interlaminar crack growth, which is influenced by the strain rate-dependent mechanical properties and fracture behaviour of the epoxy matrix [90]. Amongst the tested channels, the lamina bending observed as a result of dynamic axial loading (i.e., Figure 6.36d) differed significantly from that observed as a result of quasi-static axial loading (Figure 6.48d). Without defining the strain-rate dependent properties of the composite and by calibrating to the dynamic condition, the failure mode unique to quasi-static loading could not be predicted.

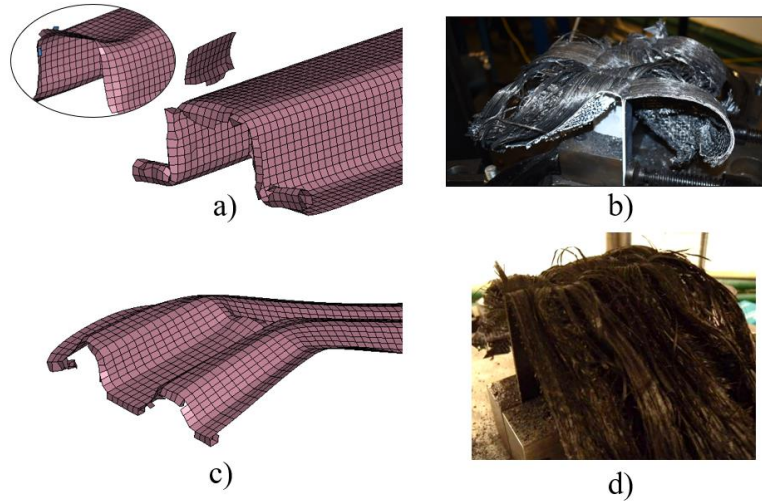


Figure 6.48: Quasi-static axial loading of $[\pm 45/0_2]_s$ hat channel: (a) Simulation model at 50 mm of impactor displacement, and (b) Post-test image [25]. Quasi-static axial loading of $[\pm 45/0_2]_s$ corrugated channel: (c) Simulation model at 23 mm of impactor displacement, and (d) Post-test image [25].

6.3.3 Discussion on Quasi-static Axial Loading of Open Channels

The simulation models utilizing the calibrated material model were unable to accurately predict the performance of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ open channels undergoing quasi-static axial loading. As observed with the dynamic axial loading simulation models, when the predominant failure mode observed in the experiments was not fragmentation, the predicted results differed notably because of the single-shell part representation. Additionally, strain-rate-dependent properties were not included in the material card definition for MAT054. As a result, changes in damage propagation as a result of changes in strain rate could not be captured with the simulation models and furthered discrepancies between predicted and average experimental performance.

6.4 Double-Hat Channel Dynamic Axial Loading Simulation Results

The force-displacement response predictions for the double-hat channels subject to dynamic axial loading are presented in this section. The calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. The predicted results were filtered at 300 Hz SAE to minimize the oscillations introduced by the quadrilateral mesh. Additionally, the simulations were run to an impactor displacement of 90 mm in accordance with the available experimental data.

6.4.1 $[0/\pm 45/90]_s$ Channels

For the dynamic axial loading of the $[0/\pm 45/90]_s$ double-hat channels, there was good agreement between the predicted and experimental results (Figure 6.49). The initial slope at the onset of crush as well as the shape of the response during progressive crushing were well captured with the simulation model. However, the total energy absorption was overpredicted as a result of the initial overprediction in peak force and over filtering until 20 mm of impactor displacement. The simulation model predicted bending of the flanges as well as brittle fragmentation, while the adhesive was predicted to fail ahead of the crash front (indicated by the red elements in Figure 6.49). Differences between the predicted and experimental average peak force, mean crush, and energy absorption over the first 90 mm of impactor displacement were +9.7%, +11.7%, and +11.1%, respectively (Figure 6.50).

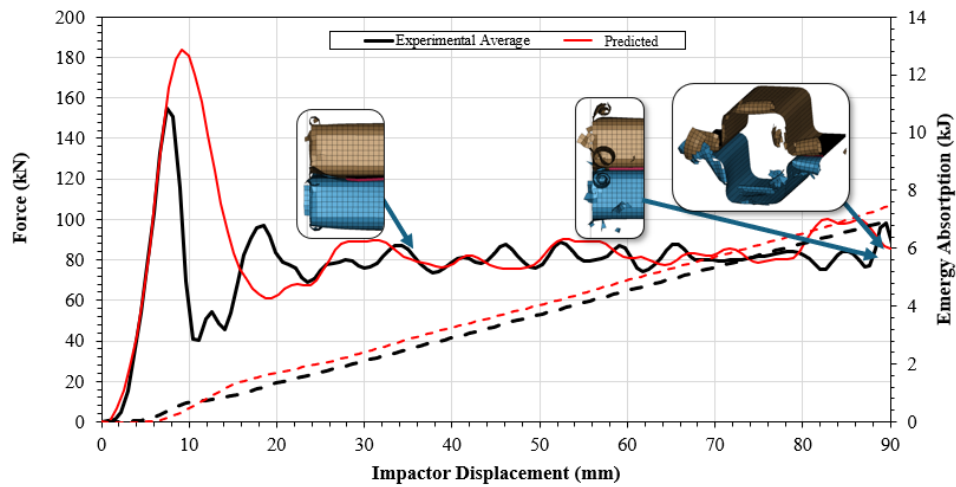


Figure 6.49: Predicted and experimental force-displacement response for $[0/\pm 45/90]_s$ double-hat channels subjected to dynamic axial loading.

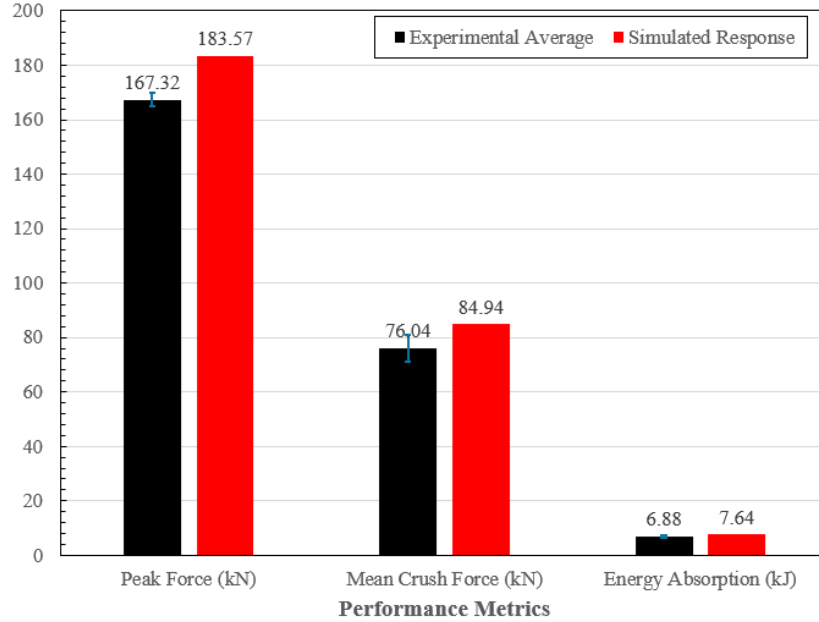


Figure 6.50: Comparison of the predicted and average experimental performance metrics for the $[0/\pm 45/90]_s$ double-hat channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 90 mm.

6.4.2 $[\pm 45/0_2]_s$ Channels

For the dynamic axial loading of the $[\pm 45/0_2]_s$ double-hat channels, there was poor agreement between the predicted and average experimental force-displacement responses (Figure 6.51). The mean crush force decreased near 40 mm of impactor displacement which resulted in an underprediction of the total energy absorption. The simulation model also predicted significant folding of the flanges as the displacement progressed and the component eventually began to bend inwards. The bending was the result of the high longitudinal compressive failure strain set for the plies in MAT_054, which allowed for further deformation in lieu of fragmentation, as was observed experimentally (Figure 5.19). Given the increased number of 0° plies, more deformation was predicted for channels with this stacking sequence than those with a quasi-isotropic stacking sequence. Once again, the adhesive was predicted to fail ahead of the crush front. Differences between the predicted and experimental average peak force, mean crush force, and energy absorption over the first 90 mm of impactor displacement were -39.6%, -21.0%, and -21.0%, respectively (Figure 6.52).

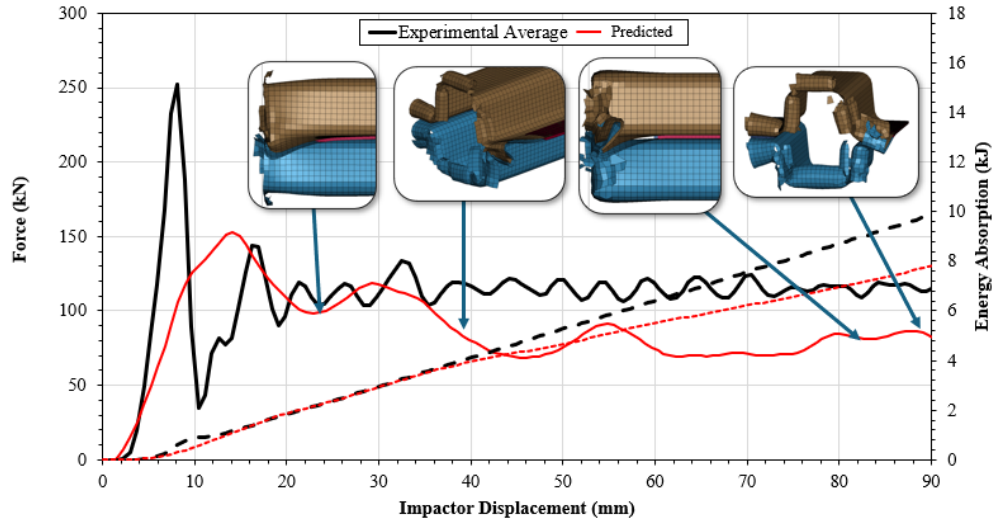


Figure 6.51: Predicted and experimental force-displacement responses for the $[\pm 45/0_2]_s$ double-hat channels subjected to dynamic axial loading.

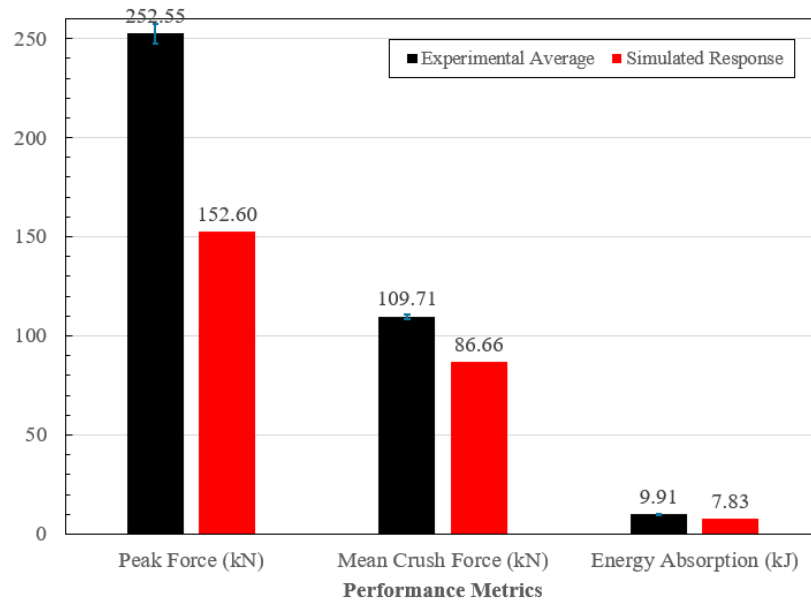


Figure 6.52: Comparison of the predicted and average experimental performance metrics of the $[\pm 45/0_2]_s$ double-hat channel under dynamic axial loading. Performance metrics determined up to an impactor displacement of 90 mm.

6.4.3 Discussion of Results for Double Hat Channel Dynamic Axial Loading

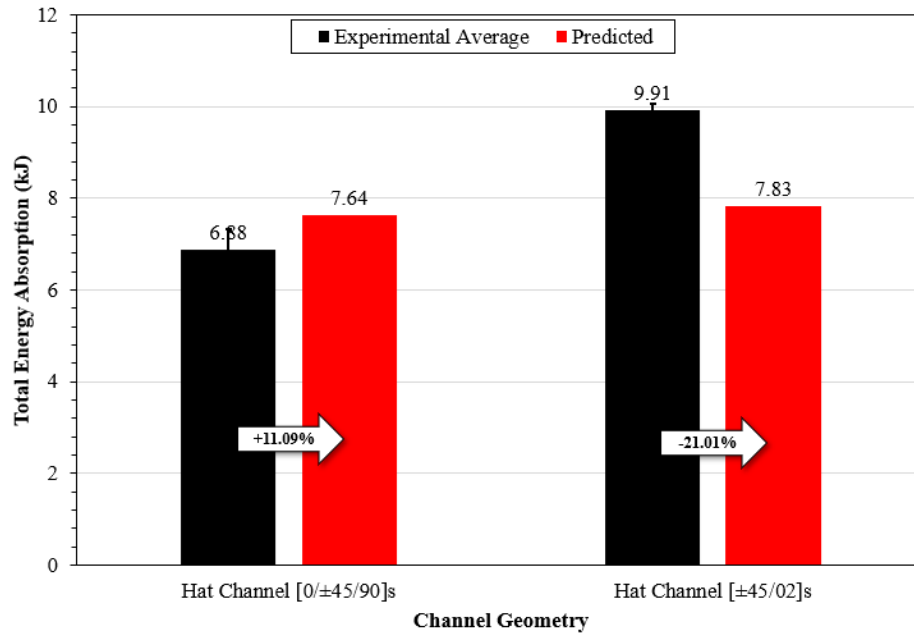


Figure 6.53: Comparison of the experimental and predicted total energy absorption for the [0/±45/90]_s and [±45/0]_{2s} double-hat channels under dynamic axial loading.

For the dynamic axial loading of the [0/±45/90]_s and [±45/0]_{2s} double-hat channels, the deviation between the predicted and average experimental total energy absorption was +11.09% and -21.01%, respectively (Figure 6.53). For the former stacking sequence, the overprediction in the predicted total energy absorption was in part due to the filtering of the results as well as the predicted bending of the flanges which was not observed experimentally (see Section 5.3.1). For the [±45/0]_{2s} double-hat channels, the total energy absorption was underpredicted due to the drop in the force at approximately 35 mm of displacement, which corresponded to local buckling of the channels. The predominant failure mode observed during the experiments was brittle fracture (Figure 6.55), which was not captured by the simulation model.

Moreover, it is worth noting that for both stacking sequences the adhesive was predicted to fail prematurely, farther ahead of the crush front. This differed from what was observed experimentally, where cracks did not propagate through the adhesive (Section 5.3). As a result, in both cases, the flanges exhibited non-physical behaviour, which may have contributed to inaccurate predictions of the total energy absorbed. Measurements of the stress along the cohesive elements

indicated that the magnitudes of shear stress were much higher than the magnitudes of normal stress (Figure 6.54). Thus, failure was the result of Mode II, sliding. For the example of cohesive element stress measurements provided, increases and decreases in stress until failure were also observed as a result of the displacement of the channel flanges and the failure of the elements ahead of this particular element. The result was loading and unloading in the cohesive element until it reached the failure stress defined for this material (~26 MPa, Table 4.4) and failed. For the $[0/\pm 45/90]_s$ double-hat channels, the flanges compressed together and rolled as opposed to fragmenting with the main section of the channel (Figure 6.55a). Similarly, for the $[\pm 45/0_2]_s$ double-hat channels, the flanges bent inwards (Figure 6.55c) as opposed to bending outwards as was observed in the experiments (Figure 6.55d). In both cases, there was minimal opening of the flanges, and the elements were compressed and sheared. Lastly, it is worth noting that the Mode II response of the adhesive was calibrated with limited success (Appendix E), which may have contributed to the prediction of premature Mode II cohesive failure.

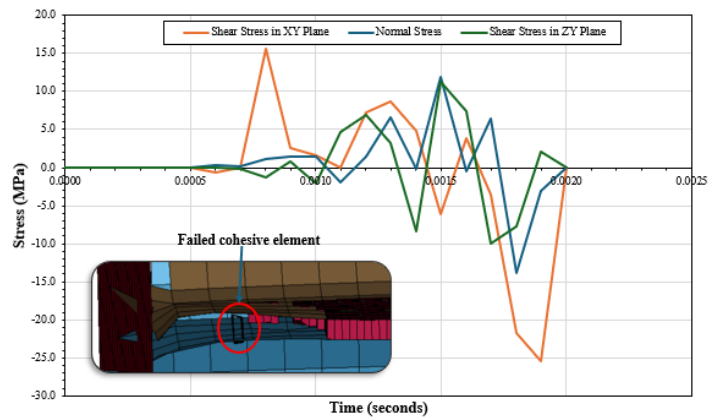


Figure 6.54: Normal and shear stresses measured in a cohesive element that failed prior to the flanges during the dynamic axial loading of a $[0/\pm 45/90]_s$ double hat channel.

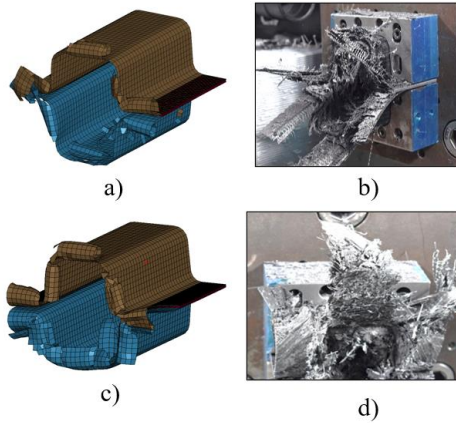


Figure 6.55: Dynamic axial loading of $[0/\pm 45/90]_s$ double-hat channel: (a) Simulation model at 71 mm of impactor displacement, and (b) Post-test image. Dynamic axial loading of $[\pm 45/0_2]_s$ double-hat channel: (c) Simulation model at 52 mm of impactor displacement, and (d) Post-test image.

6.5 Double-Hat Channel Quasi-Static Axial Loading Simulation Results

The force-displacement predictions of double-hat channels subject to quasi-static axial loading are presented in this section. The final calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. The predicted results were filtered at 5 Hz SAE to minimize the oscillations introduced by the quadrilateral mesh. Additionally, the simulations were run to an impactor displacement of 85 mm to minimize computational time.

6.5.1 $[0/\pm 45/90]_s$ Channels

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ double-hat channel, the force-displacement response was underpredicted and the simulation model predicted some bending of the channel along with fragmentation (Figure 6.56). Local bending of the flanges within the simulation model caused the adhesive elements to fail ahead of the crush front at different locations. Differences between the predicted and experimental average peak force, mean crush force, and energy absorption over the first 85 mm of impactor displacement were -22.9%, -32.8%, and -32.8%, respectively (Figure 6.57).

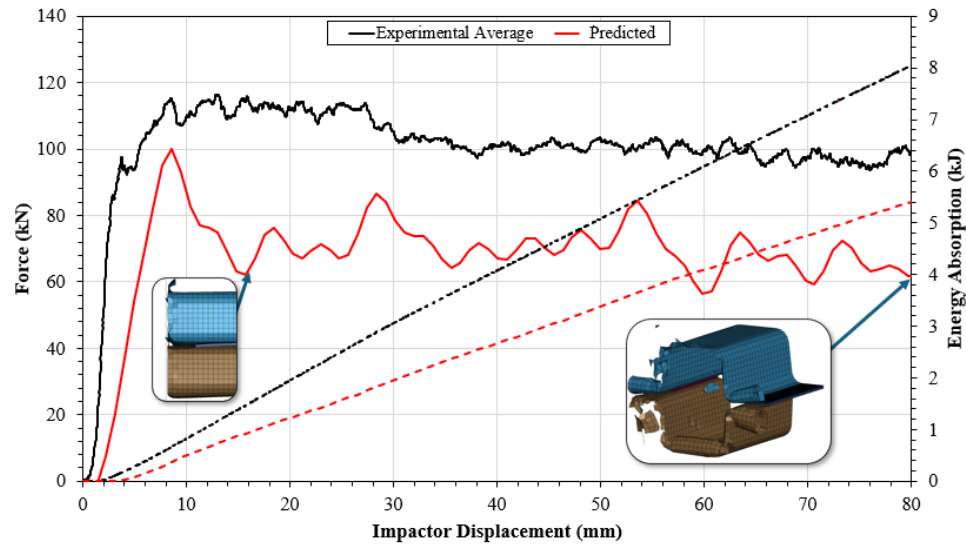


Figure 6.56: Predicted and experimental force-displacement responses of the quasi-static axial loading of $[0/\pm 45/90]_s$ double-hat channels.

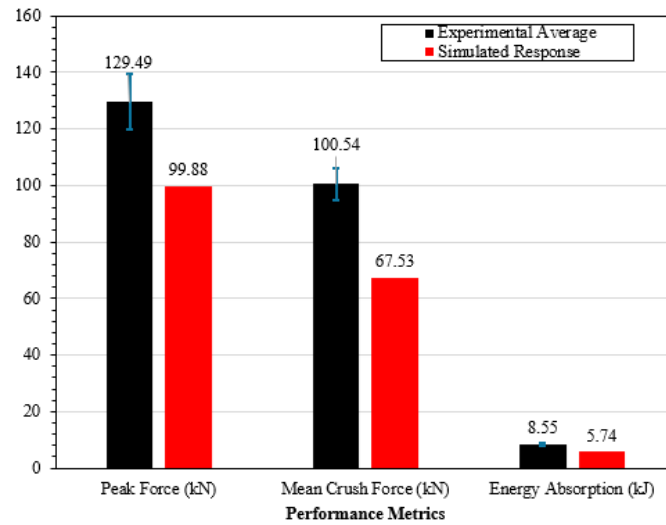


Figure 6.57: Comparison of the predicted and average experimental performance metrics of the $[0/\pm 45/90]_s$ double-hat channel under quasi-static axial loading. Performance metrics determined up to an impactor displacement of 85 mm.

6.5.2 $[\pm 45/0_2]_s$ Channels

For the quasi-static axial loading of the $[\pm 45/0_2]_s$ double-hat channel, though the magnitude of the force-displacement response was underpredicted, the overall shape was well captured (Figure

6.58). This included the prediction of the decreasing crush force that was observed for the quasi-static axial loading of hat channels of this stacking sequence only. Additionally, the simulation model predicted significant folding of the flanges, but the adhesive failed with the failure of the channel elements as opposed to ahead of the crush front. Differences between the predicted and experimental average peak force, mean crush force, and energy absorption over the first 85 mm of impactor displacement were -22.9%, -33.2%, and -34.8%, respectively (Figure 6.59).

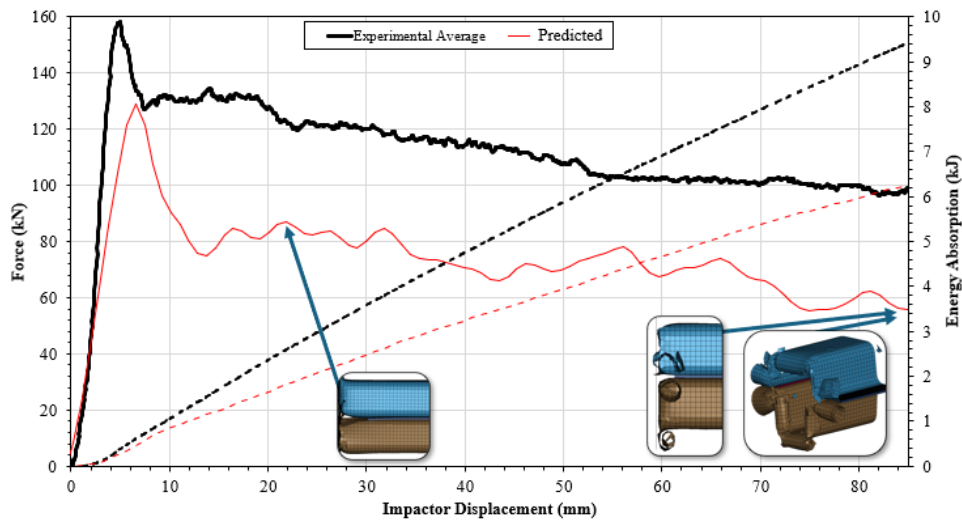


Figure 6.58: Predicted and experimental force-displacement responses of the quasi-static axial loading of $[\pm 45/0_2]_s$ double-hat channels.

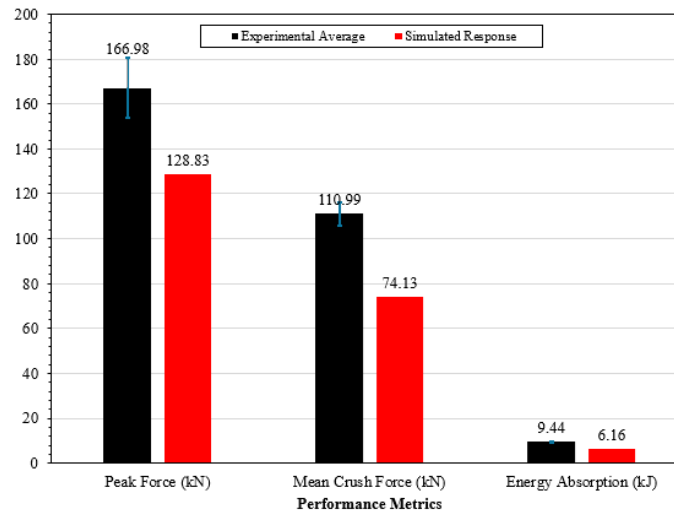


Figure 6.59: Comparison of the predicted and average experimental performance metrics of the $[\pm 45/0_2]_s$ double-hat channel under quasi-static axial loading. Performance metrics determined up to an impactor displacement of 85 mm.

6.5.3 Discussion of Results for Quasi-Static Axial Loading of Double-Hat Channel

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ double-hat channels, the deviation between the predicted and average experimental total energy absorption was -32.84% and -34.78%, respectively (Figure 6.60). The magnitude of error was similar to that observed for the simulation of the single hat channels subjected to quasi-static axial loading (Figure 6.42). For the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ double-hat channels, brittle fracture and splaying were the predominant failure modes observed in the experiments, respectively. These failure modes were not sufficiently captured by the simulation models. Instead, predictions of lamina bending were overpronounced due to premature failure of the adhesives which allowed for the propagation of cracks far ahead of the crush front and reduced the crushing of the flanges. This was distinct from the experiments where instead delamination cracks propagated between plies and remained near the crush front. The premature failure of the adhesive thus resulted in an underprediction of energy absorption. The adhesive failed prematurely because of the excessive deformation in the flanges.

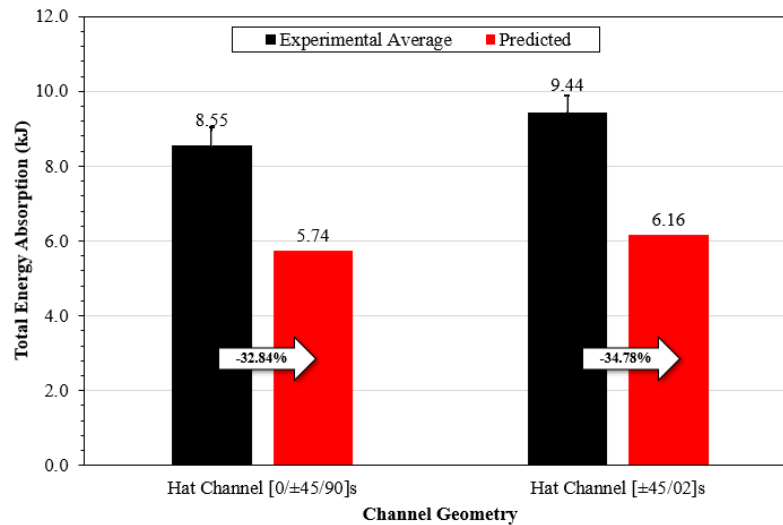


Figure 6.60: Comparison of the experimental and predicted total energy absorption for the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ double-hat channels under quasi-static axial loading.

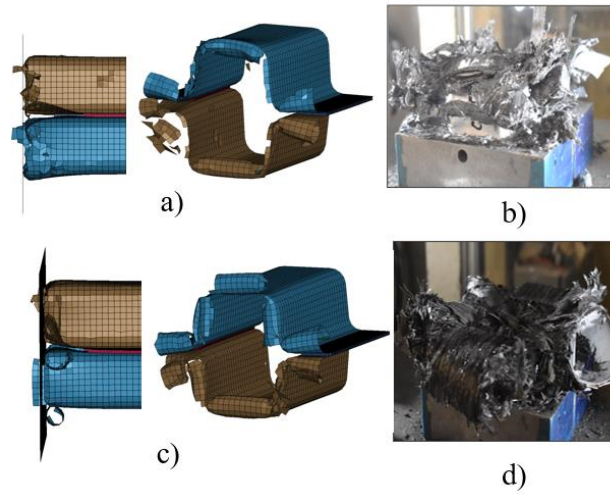


Figure 6.61: Quasi-static axial loading of $[0/\pm 45/90]_s$ double-hat channel: (a) Simulation model at 70 mm of impactor displacement, and (b) Post-test image. Quasi-static axial loading of $[\pm 45/0]_s$ double-hat channel: (c) Simulation model at 50 mm of impactor displacement, and (d) Post-test image.

6.6 Open Channel Dynamic Three-Point Bending Simulation Results

The force-displacement predictions of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels subject to quasi-static three-point bending are presented in this section. The final calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. The experimental results captured by Harvey et al. [25] with the same geometry, material, and loading conditions were provided for comparison in each figure.

Three-point bending tests provide an indication of the resistance of a component to penetration. Thus, the performance metrics presented for comparison between the experimental and predicted results are peak force and energy absorption up to the peak force.

6.6.1 $[0/\pm 45/90]_s$ Channels

Only the predicted results for the corrugated channel under dynamic three-point bending are presented in this section. The corresponding experiments performed on the hat channels of the same stacking sequence did not fail by penetration of the impactor into the channel. Instead, the channel opened and flattened [25]. As a result, a simulation model for this condition was not created.

6.6.1.1 Corrugated Channel

For the dynamic three-point bending of the $[0/\pm 45/90]_s$ corrugated channel, the initial slope of the force-displacement response was well predicted; however, the performance of the channel was overpredicted as can be observed by the force-displacement response (Figure 6.62). Nevertheless, the channel was predicted to fail via brittle fracture at the location of impact which was reflective of the experiments [25]. The experimental average peak force was 6.06 ± 0.36 kN, while the predicted peak force (unfiltered) was 9.01 kN, for a discrepancy of +48.6% (Figure 6.63). The average experimental and predicted energy absorptions up to the peak force were 0.053 kJ and 0.095 kJ, respectively, for a discrepancy of +78.4% (Figure 6.63).

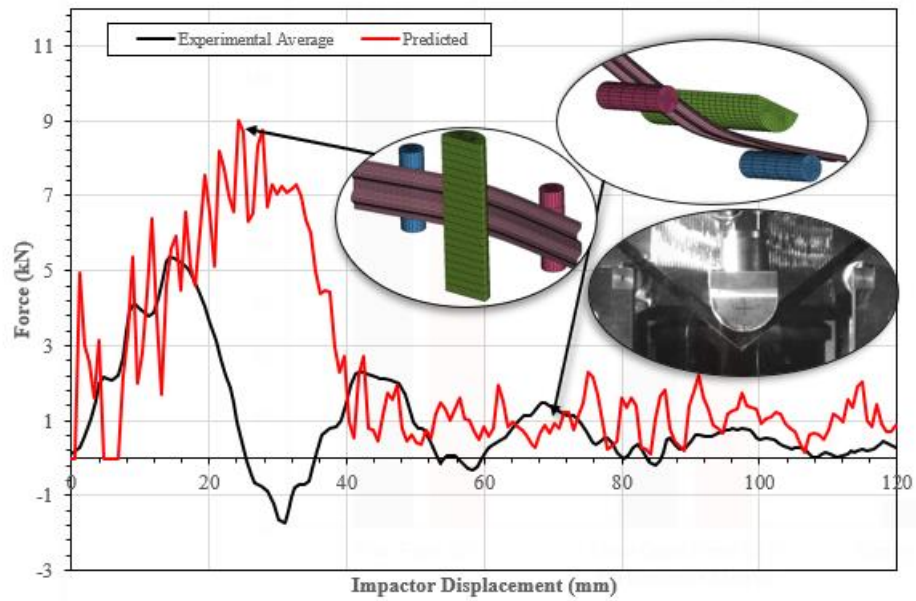


Figure 6.62: Predicted and experimental [25] force-displacement responses of the dynamic three-point bending of the $[0/\pm45/90]_s$ corrugated channels.

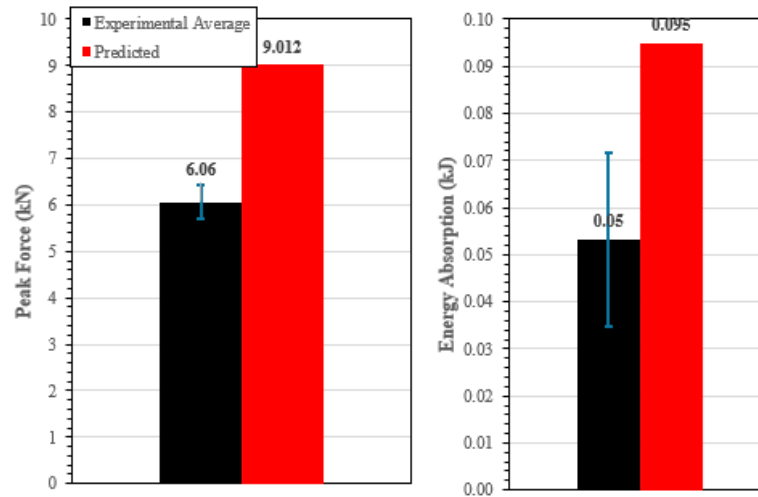


Figure 6.63: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm45/90]_s$ corrugated channels under dynamic three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 15 mm [25] and predicted impactor displacement of 24 mm).

6.6.2 $[\pm 45/0_2]_s$ Channels

6.6.2.1 Hat Channel

For the dynamic three-point bending of the $[\pm 45/0_2]_s$ hat channels, the force-displacement response was once again overpredicted despite possessing good agreement with the initial slope at the onset of crushing. Additionally, some fracture was predicted at the location of the impactor as observed in the experiments (Figure 6.64). The experimental average peak force was 11.90 ± 0.99 kN, while the predicted peak force (unfiltered) was 18.9 kN, for a discrepancy of 58.9% (Figure 6.65). The average experimental and predicted energy absorptions up to the peak force were 0.047 kJ and 0.30 kJ, respectively, a discrepancy of 539.7%.

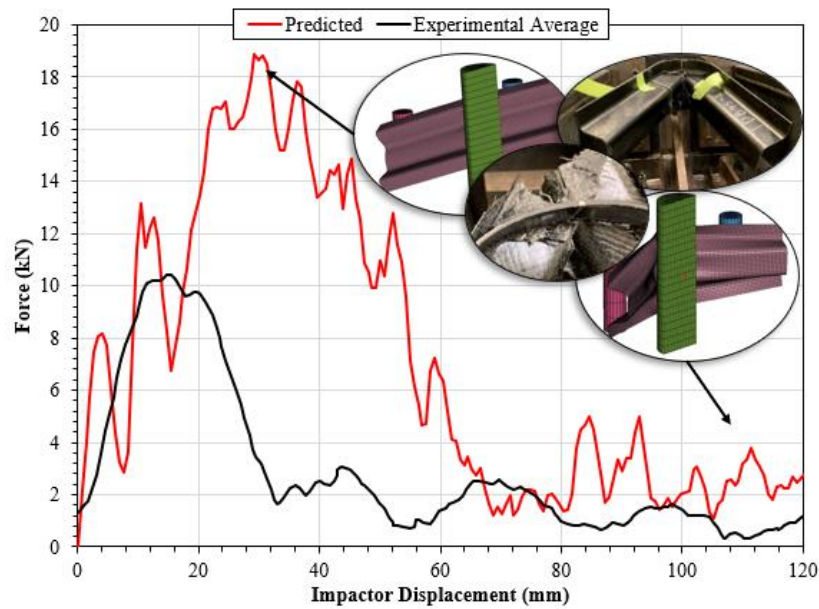


Figure 6.64: Predicted and experimental [25] force-displacement responses of the dynamic three-point bending of the $[\pm 45/0_2]_s$ hat channels.

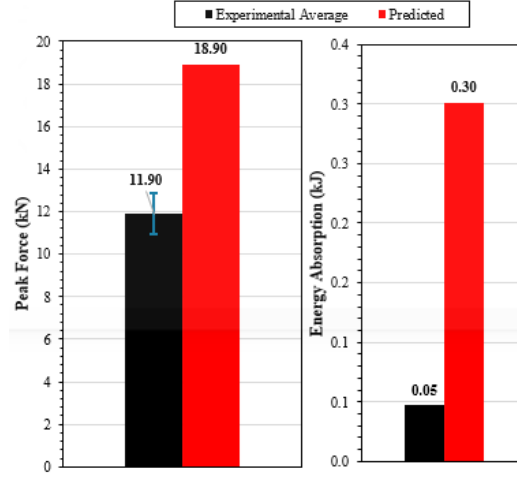


Figure 6.65: Comparison of the predicted and average experimental [25] performance metrics of the $[\pm 45/0_2]_s$ hat channels under dynamic three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 11 mm [25] and predicted impactor displacement of 29 mm).

6.6.2.2 Corrugated Channel

For the dynamic three-point bending of the $[\pm 45/0_2]_s$ corrugated channel, the force-displacement response was again overpredicted and failure via brittle fracture at the location of the impactor was predicted, which was consistent with the experiments (Figure 6.66). The experimental average peak force was 6.98 ± 0.29 kN, while the predicted peak force (unfiltered) was 7.91 kN, for a discrepancy of +13.4% (Figure 6.67). The average experimental energy absorption until the peak force was 0.030 kJ while the predicted energy absorption was 0.11 kJ for a discrepancy 259.0%.

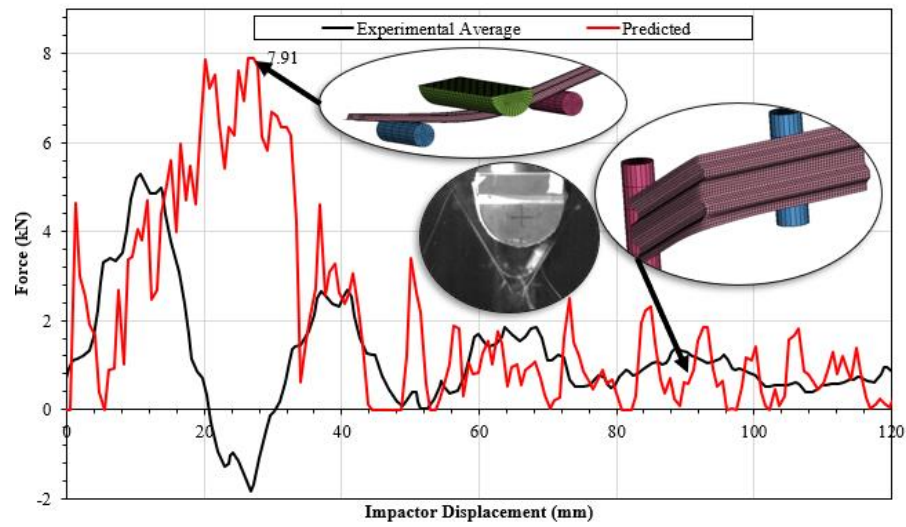


Figure 6.66: Predicted and experimental [25] force-displacement responses of the dynamic three-point bending of the $[\pm 45/0_2]_s$ corrugated channels.

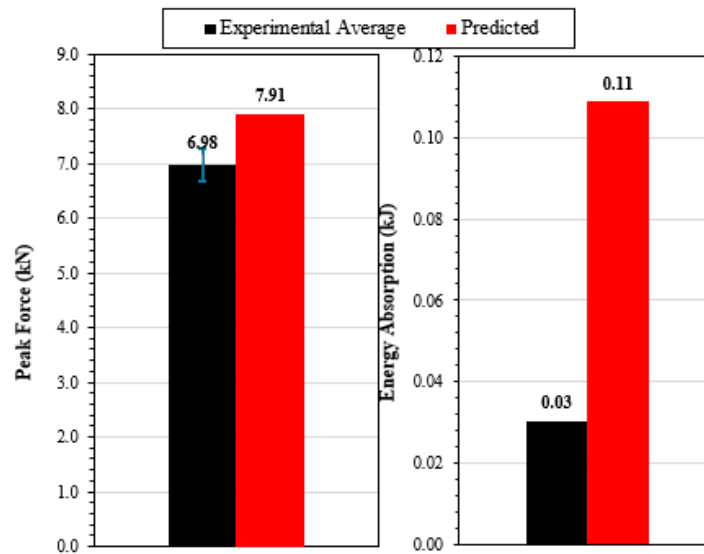


Figure 6.67: Comparison of the predicted and average experimental [25] performance metrics of the $[\pm 45/0_2]_s$ corrugated channels under dynamic three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 11 mm [25] and predicted impactor displacement of 26 mm).

6.6.3 Discussion on Dynamic Three-Point Bending

The error between the experimental [25] and predicted peak forces for channels subjected to dynamic three-point bending are presented (Figure 6.68). For all simulations, the peak force was overpredicted. In the case of dynamic loading, the errors were +48.60%, +58.85%, and +13.39%, for the $[0/\pm 45/90]_s$ corrugated channel, the $[\pm 45/0_2]_s$ hat channel, and the $[\pm 45/0_2]_s$ corrugated channel, respectively. The discrepancies between the experimental [25] and predicted energy absorption until peak force are also presented (Figure 6.69). The errors were +78.41%, +539.67%, and +259.04%, for the $[0/\pm 45/90]_s$ corrugated channel, the $[\pm 45/0_2]_s$ hat channel, and the $[\pm 45/0_2]_s$ corrugated channel, respectively. The overprediction in peak force resulted in an overprediction in energy absorption as the area under the simulated force-displacement graph for each condition was much larger than their experimental counterpart. While images from the experiment indicated that some delamination was present, failure was predominantly captured through the tensile failure of the lowermost plies of the channels. Thus, for this loading condition, fibre tensile failure strain (DFAILT) should be the most influential parameter in the MAT_054 calibration for this set of simulation models. To demonstrate this, DFAILT was adjusted for the simulation model of the $[0/\pm 45/90]_s$ corrugated channel subject to dynamic three-point bending (recalling that the hat channel of this stacking sequence does not have experimental results available) while all other calibrated values remained constant (Figure 6.70).

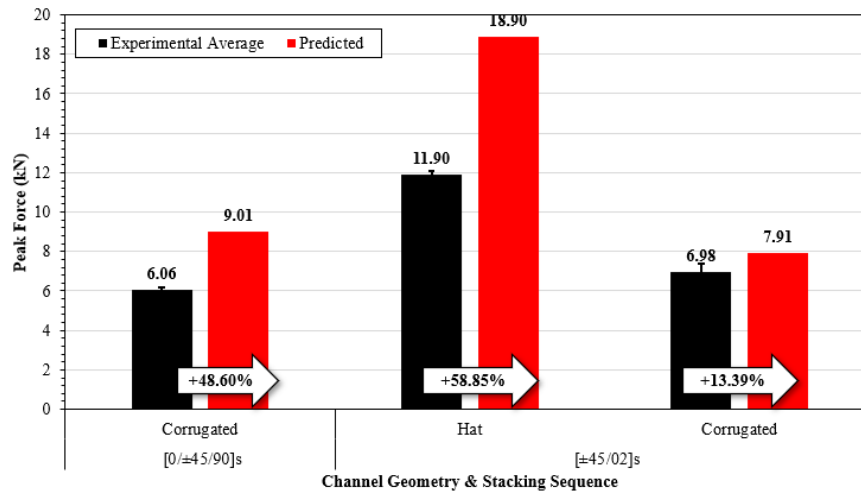


Figure 6.68: Comparison of the experimental and predicted peak force of the $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ open channels subject to dynamic three-point bending. Note, the experimental results were taken from [25].

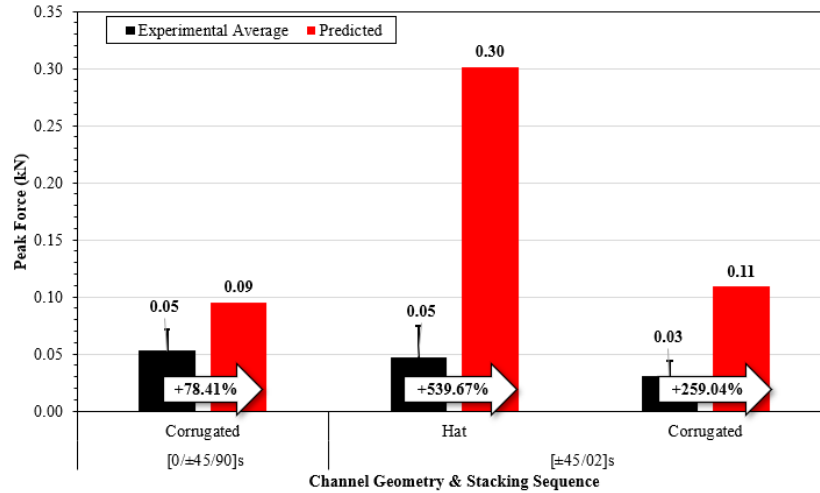


Figure 6.69: Comparison of the experimental [25] and predicted energy absorption until peak force of [0/±45/90]_s and [±45/02]_s open channels subject to dynamic three-point bending.

For a decrease in the DFAILT, the peak force decreased proportionally. The DFAILT value which best approximated the experimental peak force for this geometry, stacking sequence and loading condition was found to be DFAILT = 0.01. However, when this value was applied to the simulation model for the baseline case, the simulation model predicted the premature failure of the hat channel at the constraints representing the fixture (Figure 6.71). This result highlights a limitation with MAT_54 and other available material models, namely that the calibration of non-physical parameters for one loading condition (i.e., axial crush) does not necessarily translate well to a different loading condition where distinct failure mechanisms are observed.

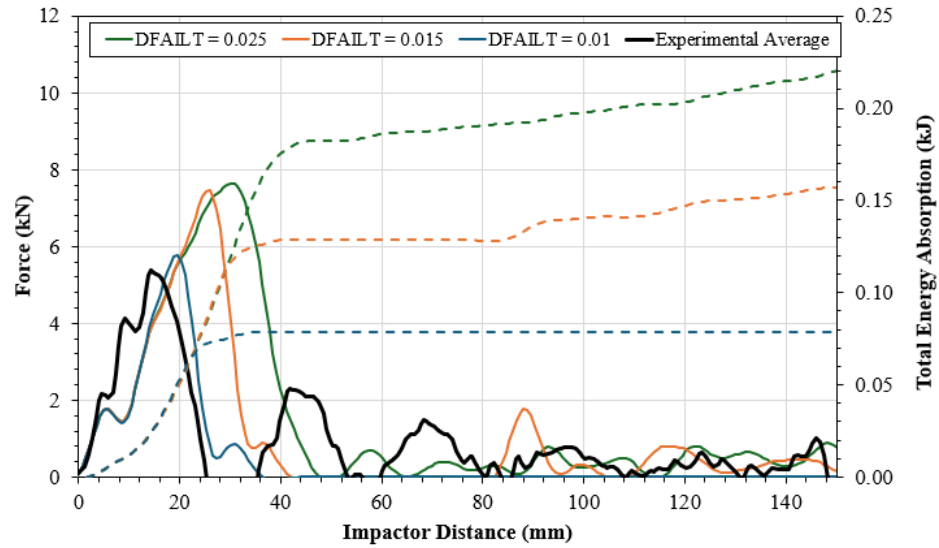


Figure 6.70: Comparison of the average experimental [25] force-displacement response of the $[0/\pm 45/90]_s$ corrugated channel subject to dynamic three-point bending to the simulated force-displacement responses with DFAILT in MAT_054 adjusted to 0.025, 0.015, and 0.01.

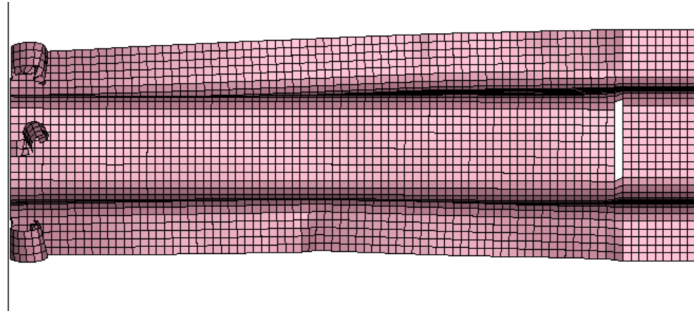


Figure 6.71: Simulated behaviour of the $[0/\pm 45/90]_s$ hat channel subject to dynamic axial loading with DFAILT in MAT_054 adjusted to 0.01.

Besides this, the geometry-dependent channel behaviour observed with the axial loading simulations remained present for the three-point bending loading conditions. For example, the over-pronounced local bending behaviour observed for the $[\pm 45/0_2]_s$ hat channel under axial loading was observed during the dynamic three-point bending simulations with the opening of the channel cross-section and bending of the flanges in place of fracture (Figure 6.72e). Meanwhile, the corrugated channel continued to exhibit brittle behaviour consistent with the axial loading simulations (Figure 6.72d, f).

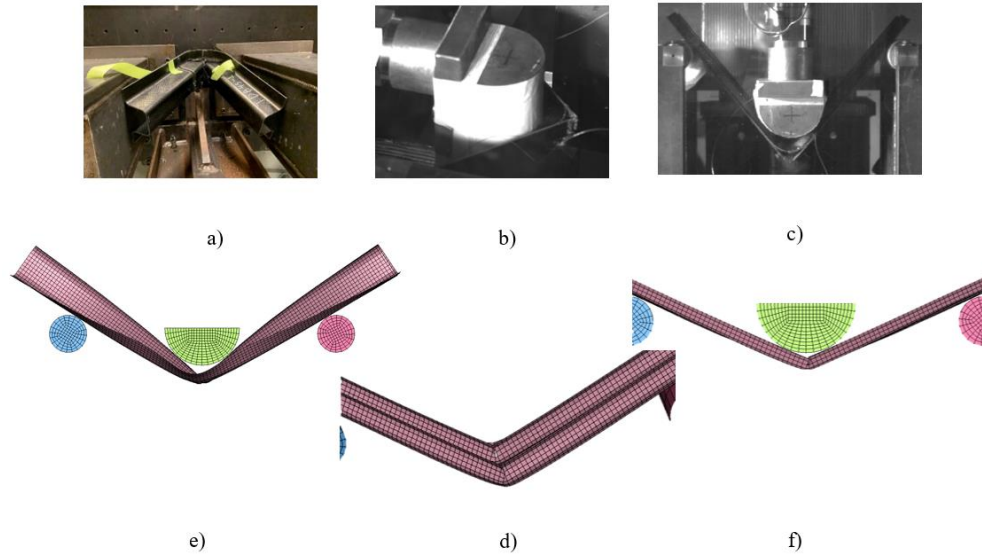


Figure 6.72: Dynamic three-point bending specimens captured post-test [25]: (a) $[\pm 45/0_2]_s$ hat channel, (b) $[0/\pm 45/90]_s$ corrugated channel, and (c) $[\pm 45/0_2]_s$ corrugated channel. Predicted dynamic three-point bending specimen deformation captured at end of simulation run: (d) $[\pm 45/0_2]_s$ hat channel, (e) $[0/\pm 45/90]_s$ corrugated channel, and (f) $[\pm 45/0_2]_s$ corrugated channel.

6.7 Open Channel Quasi-Static Three-Point Bending Simulation Results

The force-displacement predictions of $[0/\pm 45/90]_s$ and $[\pm 45/0_2]_s$ hat and corrugated channels subject to quasi-static three-point bending are presented in this section. The final calibrated MAT_054 non-physical parameters (Table 6.5) were used for all simulations. As with the dynamic three-point bending results, the performance metrics presented for comparison are peak force and energy absorption up to the peak force.

6.7.1 $[0/\pm 45/90]_s$ Channels

6.7.1.1 Hat Channels

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ hat channel, the performance of the channel was overpredicted and the channel was predicted to fracture slightly. The experimental average peak force was 9.37 kN, while the predicted peak force (unfiltered) was 16.77 kN, for a discrepancy of +78.98% (Figure 6.73). The average experimental and predicted energy absorptions up to the peak force were 0.10 kJ and 0.48 kJ, respectively, for a discrepancy of +57.62% (Figure

6.74). Only one trial was completed experimentally [25] due to the undesirable failure mode observed. Harvey et al. [25] reported that as opposed to fracturing, the channel opened and thus subsequent trials were not completed. It can be observed that a similar opening mode was predicted by the simulation model but with more deformation at the impactor location.

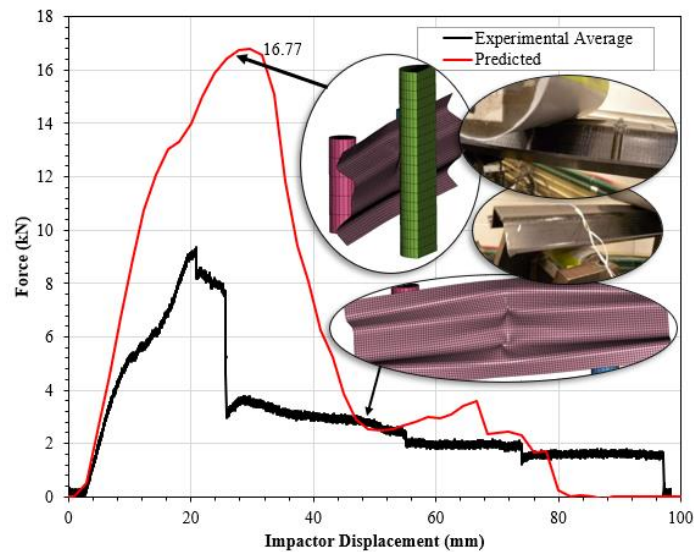


Figure 6.73: Predicted and experimental [25] force-displacement responses of the quasi-static three-point bending of the $[0/\pm 45/90]_s$ hat channels.

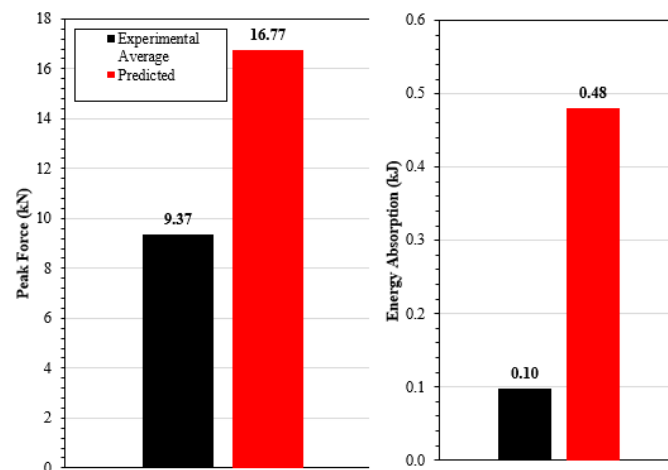


Figure 6.74: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm 45/90]_s$ hat channel under quasi-static three-point bending. Performance metrics were

determined up to the peak force (average experimental impactor displacement of 21mm [25] and predicted impactor displacement of 30 mm).

6.7.1.2 Corrugated Channels

For the quasi-static axial loading of the $[0/\pm 45/90]_s$ corrugated channel, the performance of the channel was overpredicted and the channel was predicted to fail via a brittle fracture. The experimental average peak force was $4.96 \text{ kN} \pm 0.12 \text{ kN}$, while the predicted peak force (unfiltered) was 7.47 kN , for a discrepancy of $+50.66\%$ (Figure 6.75). The average experimental and predicted energy absorptions up to the peak force were $0.04 \text{ kJ} \pm 0.003 \text{ kJ}$ and 0.07 kJ , respectively, for a discrepancy of $+78.11\%$ (Figure 6.76). As can be observed, the predicted and experimental failure modes aligned well within the constraints of a single shell model.

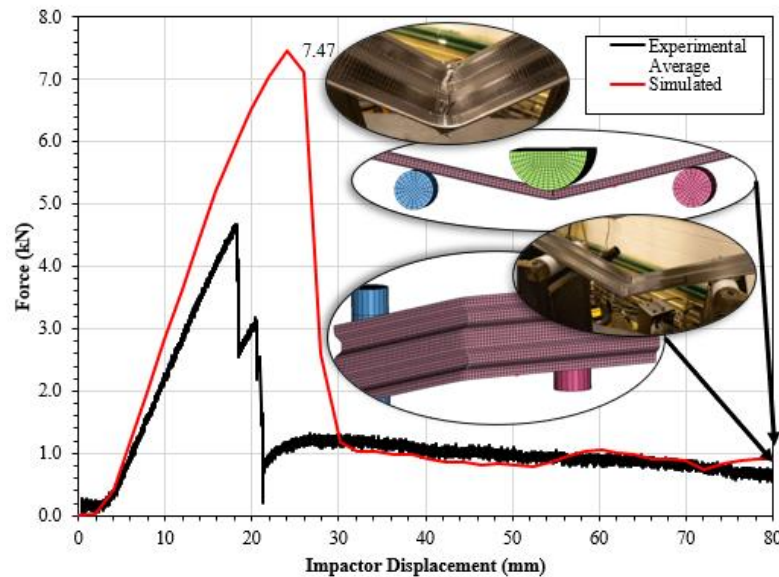


Figure 6.75: Predicted and experimental [25] force-displacement responses of the quasi-static three-point bending of the $[0/\pm 45/90]_s$ corrugated channels.

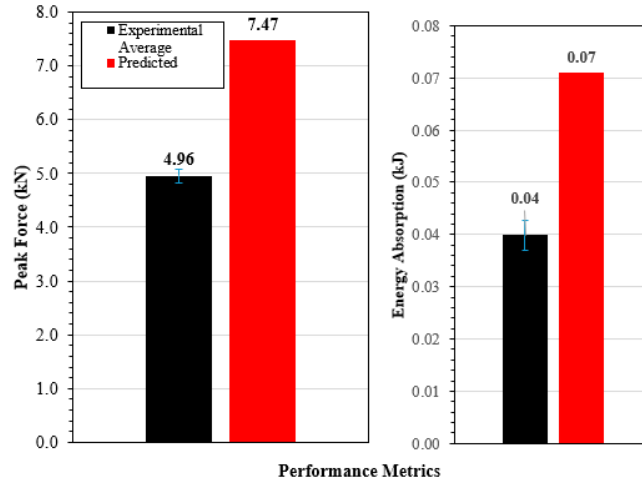


Figure 6.76: Comparison of the predicted and average experimental [25] performance metrics of the $[0/\pm 45/90]_s$ corrugated channel under quasi-static three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 18 mm [25] and predicted impactor displacement of 22 mm).

6.7.2 $[\pm 45/0_2]_s$ Channels

6.7.2.1 Hat Channels

For the quasi-static axial loading of the $[\pm 45/0_2]_s$ hat channel, the performance of the channel was overpredicted but the failure behaviour (bending near the location of the impactor) was reasonably captured as per the experimental observations of Harvey et al. [25]. The experimental average peak force was $9.85 \text{ kN} \pm 0.16 \text{ kN}$, while the predicted peak force (unfiltered) was 16.28 kN, for a discrepancy of +51.11% (Figure 6.77). The average experimental and predicted energy absorptions up to the peak force were $0.12 \text{ kJ} \pm 0.003 \text{ kJ}$ and 0.23 kJ, respectively, for a discrepancy of +95.33 % (Figure 6.78).

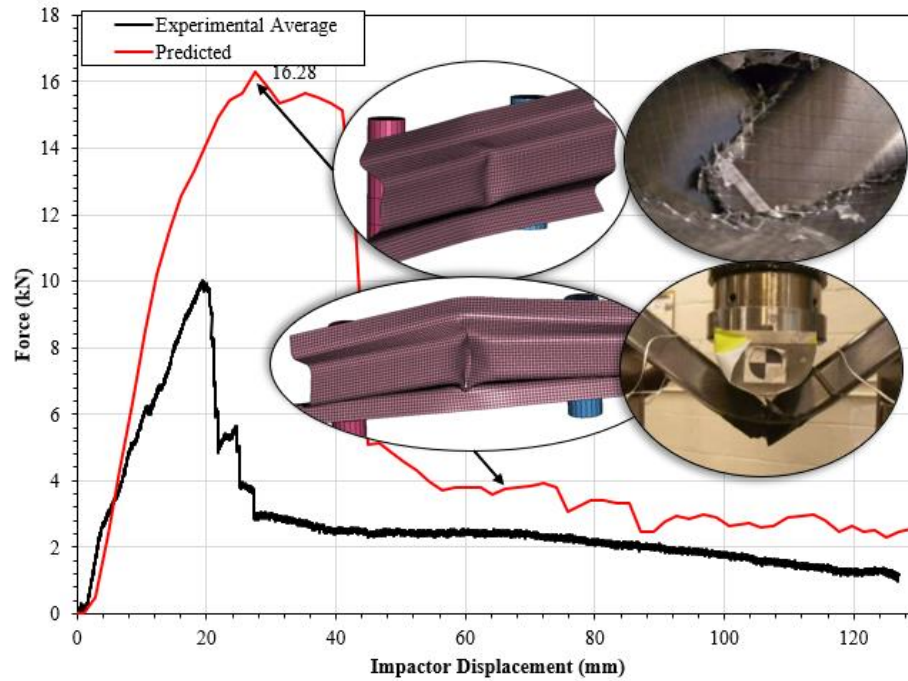


Figure 6.77: Predicted and experimental [25] force-displacement responses of the quasi-static three-point bending of the $[\pm 45/0_2]_s$ hat channels.

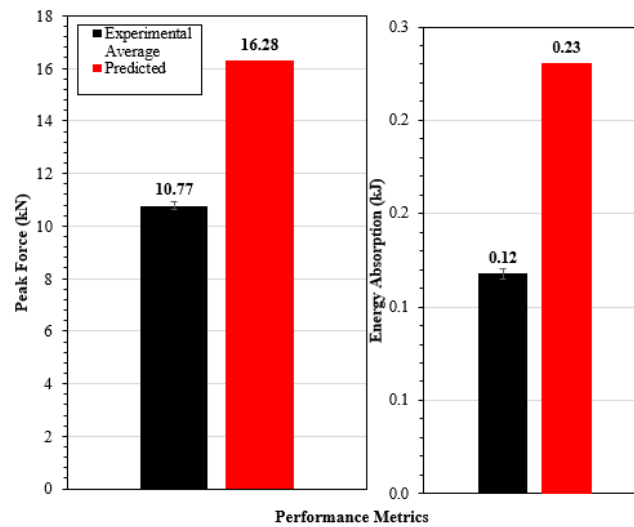


Figure 6.78: Comparison of the predicted and average experimental [25] performance metrics of the $[\pm 45/0_2]_s$ hat channel under quasi-static three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 21 mm [25] and predicted impactor displacement of 26 mm).

6.7.2.2 Corrugated Channels

For the quasi-static axial loading of the $[\pm 45/0_2]_s$ corrugated channel, the performance of the channel was overpredicted, but the failure mode predicted reasonably replicated experimental observations as brittle fracture near the location of the impactor. The experimental average peak force was $4.39 \text{ kN} \pm 0.38 \text{ kN}$, while the predicted peak force (unfiltered) was 6.46 kN , for a discrepancy of $+47.08\%$ (Figure 6.79). The average experimental and predicted energy absorptions up to the peak force were $0.02 \text{ kJ} \pm \text{kN}$ and 0.004 kJ , respectively, for a discrepancy of $+120.6 \%$ (Figure 6.80).

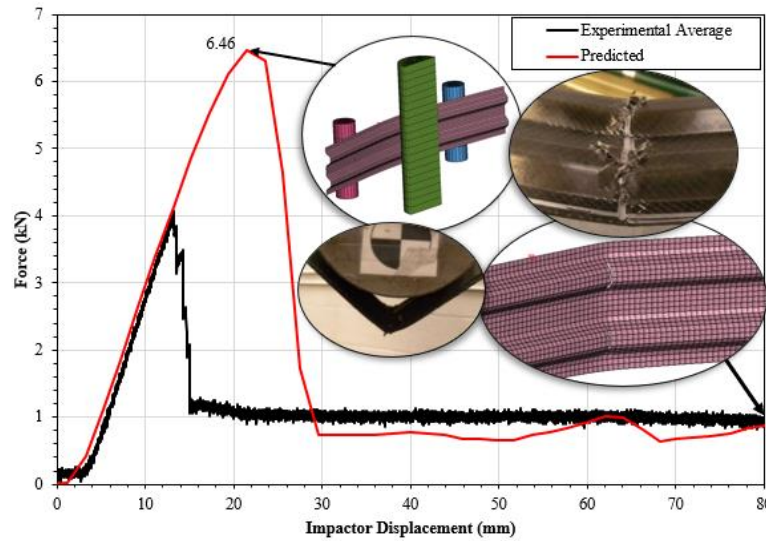


Figure 6.79: Predicted and experimental [25] force-displacement responses of the quasi-static three-point bending of the $[\pm 45/0_2]_s$ corrugated channels.

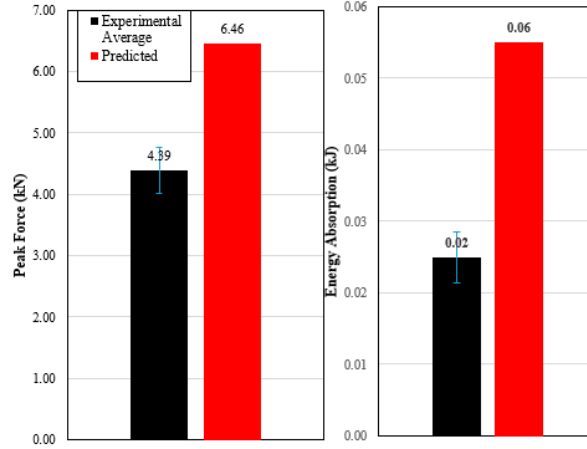


Figure 6.80: Comparison of the predicted and average experimental [25] performance metrics of the $[\pm 45/0_2]_s$ corrugated channel under quasi-static three-point bending. Performance metrics were determined up to the peak force (average experimental impactor displacement of 15 mm [25] and predicted impactor displacement of 57 mm).

6.7.3 Discussion on Quasi-static Three-Point Bending

The error between the experimental [25] and predicted peak forces for channels subjected to quasi-static three-point bending results are presented (Figure 6.81). In the case of quasi-static loading, the errors were +50.66%, +51.11%, and +47.08%, for the $[0/\pm 45/90]_s$ corrugated channel, the $[\pm 45/0_2]_s$ hat channel, and the $[\pm 45/0_2]_s$ corrugated channel, respectively. The discrepancies between the experimental [25] and predicted energy absorptions until peak force are also presented (Figure 6.82). The errors were +78.41%, +95.33%, and +120.64%, for the $[0/\pm 45/90]_s$ corrugated channel, the $[\pm 45/0_2]_s$ hat channel, and the $[\pm 45/0_2]_s$ corrugated channel, respectively. The overprediction in peak force resulted in an overprediction in energy absorption as the area under the simulated force-displacement graph for each condition was much larger than their experimental counterpart. While images from the experiment indicated that some delamination was present at failure, it predominantly occurred through the tensile failure of the lowermost plies of the channels (Figure 6.83). As discussed with the dynamic simulation models, changes in the DFAILT parameter would reduce the magnitude of overprediction for the quasi-static simulation results.

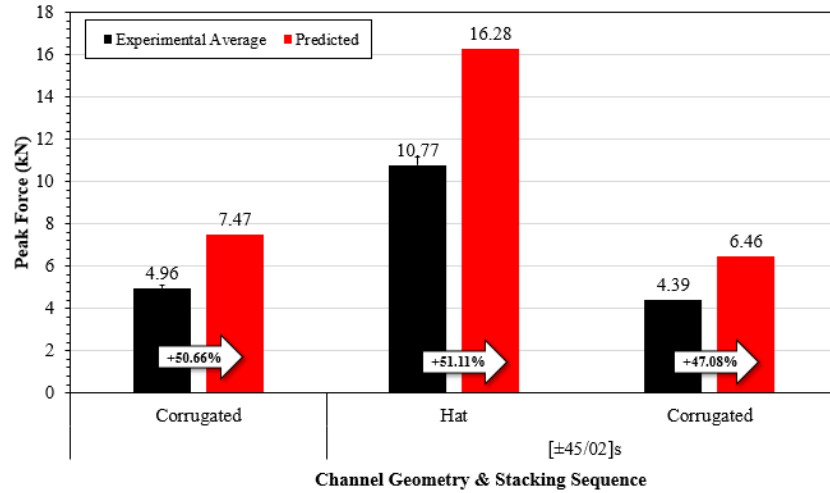


Figure 6.81: Comparison of the experimental [25] and predicted peak force of the $[0/\pm 45/90]_s$ and $[\pm 45/0]_s$ channels under quasi-static three-point bending.

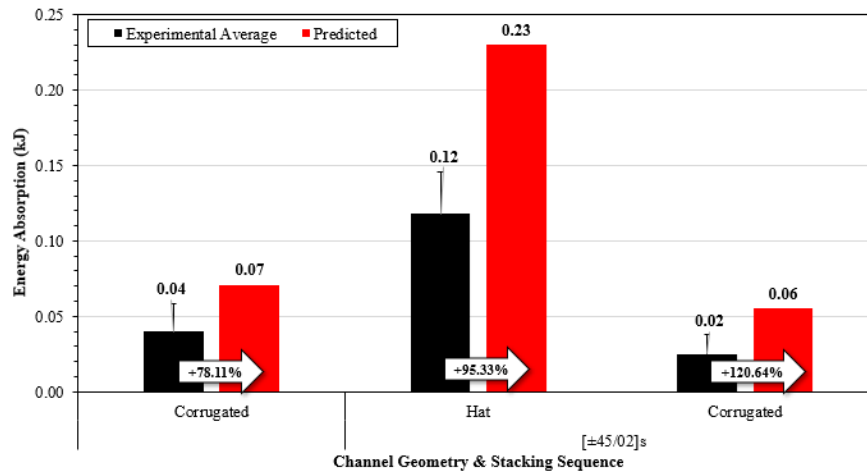


Figure 6.82: Comparison of the experimental [25] and predicted energy absorption of the $[0/\pm 45/90]_s$ and $[\pm 45/0]_s$ channels subject to quasi-static three-point bending.

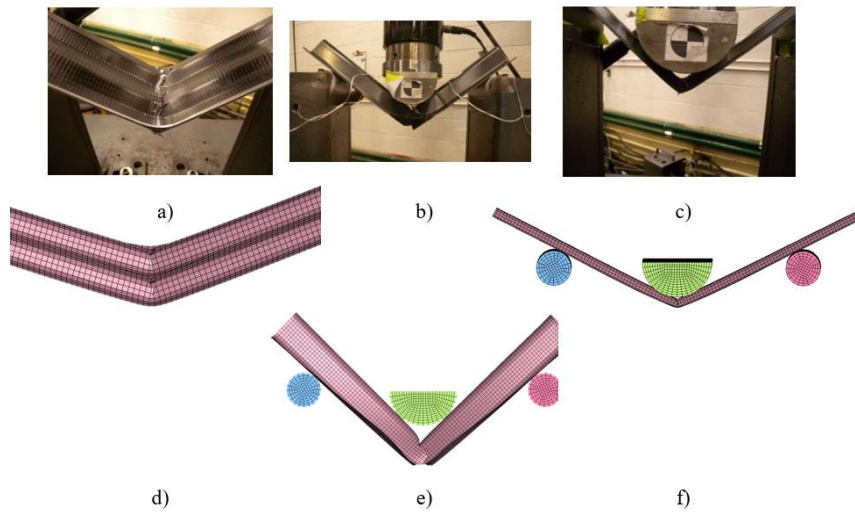


Figure 6.83: Quasi-static three-point bending specimens captured post-test [25]: (a) $[0/\pm45/90]_s$ corrugated channel, (b) $[\pm45/0_2]_s$ hat channel, and (c) $[\pm45/0_2]_s$ corrugated channel. Predicted quasi-static three-point bending specimen deformation captured at end of simulation run: (d) $[0/\pm45/90]_s$ corrugated channel, (e) $[\pm45/0_2]_s$ hat channel, and (f) $[\pm45/0_2]_s$ corrugated channel.

Chapter 7

Conclusion and Recommendations

7.1 Outcomes of Study

The goal of this study was to support the development of a high-fidelity impact simulation model to predict the energy absorption capacity of unidirectional non-crimp fabric (UD-NCF) carbon fibre-reinforced epoxy structures. Improved impact simulation models would aid the adoption of composite materials as load-bearing structures in vehicles by reducing the time and cost associated with the development of new components. This study comprised three objectives. The first objective was to characterize the energy absorbing capacity (EAC) and failure modes of UD-NCF tapered hat carbon fibre-reinforced plastic (CFRP) channels manufactured via high-pressure resin transfer (HP-RTM) under sustained axial loading. The second objective focused on developing a set of simulation models to predict the performance of the experimentally tested channels as well as previously tested channels with different stacking sequences, geometries, and loading conditions to investigate the robustness of an available material model, MAT_054, in commercial finite element software LS-DYNA. The final objective was to characterize the energy-absorbing capacity and failure behaviour of adhesively bonded channels and develop corresponding simulation models to further evaluate the robustness of the calibrated material model, MAT_054.

To achieve the first objective, $[0/\pm 45/90]_s$, $[0/90/\pm 45]_s$, and $[\pm 45/0_2]_s$ tapered hat channels were tested on a crash sled under axial compressive loading with an initial velocity of 7.5 m/s. Measurements were taken for force and displacement to calculate energy absorption, and the results were then compared to the results of previously performed tests for other open channels of the same material system. The following conclusions were drawn:

- Stacking sequence was the most influential parameter in channel performance. Stiffer channels, i.e. $[\pm 45/0_2]_s$, consistently absorbed more energy per channel geometry tested.
- The influence of geometry could not confidently be determined. Channels with the tapered channel geometry demonstrated 25% less specific energy absorption when compared to channels with different geometries and the same stacking sequence. The geometry possessed manufacturing-induced defects (fibre misalignment) near the taper,

which undoubtedly limited SEA as was evident when comparing the first 200 mm of the tapered channel to the hat channel. However, the failure modes also presented differently with the change in geometry and may have also limited specific energy absorption. This influence cannot be distinguished from the impact of the defects with the available experimental data.

- All possible failure modes generally described in the literature were exhibited during the testing of the tapered channels. Splaying of the $[\pm 45/0_2]_s$ channels corresponded to the greatest amount of energy absorption (24% to 29% increase in SEA in comparison to quasi-isotropic stacking sequences).

Towards achieving the second objective, a simulation model of the baseline condition (dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel) was created for the calibration of the non-physical parameters of the material model, MAT_054. Various contact cards, part representations, and non-physical controls were also considered to achieve numerically stable simulations. A parametric investigation varying the non-physical parameters of the material model for both structured quadrilateral and angled quadrilateral mesh structures was then completed. After calibration, the subsequent simulation models were developed for the dynamic/quasi-static axial loading/three-point bending of $[\pm 45/0_2]_s/[0/\pm 45/90]_s$ hat and corrugated channels, as well as simulation models for the open channels tested for objective 1. The following conclusions were drawn:

- The non-physical parameters of MAT_054 were calibrated to agree with average experimental data for the dynamic axial loading of a UD-NCF composite $[0/\pm 45/90]_s$ hat channel. A final discrepancy of 0.14% from the average experimental data of the baseline condition was achieved. For this calibration, DFAILC, the compressive failure to strain, and SOFT, the crush front softening parameter were the most influential parameters in achieving the desired mean crush force, with there being a trade-off in terms of brittle behaviour as compressive failure strain is increased. Where a proportional increase in DFAILC and a decrease in SOFT would yield very similar results, an effort should be made to decrease DFAILC and increase SOFT. However, the selection of DFAILT, DFAILM, YCFAC, and SLIMx parameters distinguished between predictions of progressive crushing, and catastrophic failure or non-physical deformation.

- To calibrate MAT_054 to an experimental average, the experimentally derived values can be provided for the material inputs where available (material properties, material strengths, failure strains). A time step below which elements are deleted due to extensive distortion (TFAIL) should be provided to increase the stability of the simulation model. If the simulation model provides unstable results, subsequent trials with increasing values of SOFT should be tested up to a value of 0.9. If stable crushing is still not achieved, calibration with equivalent failure strain (EFS) should be attempted with increasing values of EFS. Lastly, if EFS is not able to generate stable crushing simulations, then the mode-dependent failure strains (DFAILx) should be independently calibrated instead of EFS. After a simulation model with stable crushing has been achieved, adjustments to minimize the magnitude of difference between the predicted and experimental results can be accomplished by varying the SOFT and SLIM parameters. Once MAT_054 is calibrated to a specific condition, simulation predictions for channels with the same stacking sequence and loading condition but with different cross-sectional geometries are reasonably accurate.
- Simulation models of the axial loading of the corrugated channels using the calibrated MAT_054 consistently resulted in the least amount of non-physical deformation or local buckling across all loading conditions and stacking sequences.
- For axial loading cases when splaying (delamination) was observed, the single layer simulation model using the calibrated material model was unable to accurately capture the failure mode and, thus, the specific energy absorption. This was particularly evident for dynamic axial loading of hat channels with a [$\pm 45/02$]s stacking sequence, as well as for quasi-static axial loading cases. The fact that the material model MAT_054 did not capture the strain rate-dependent material response may have contributed to inaccurate predictions for the quasi-static axial loading cases.
- For the three-point bending cases considered, the simulation model using the calibrated material model was unable to capture the response and performance of all channels regardless of the stacking sequence, cross-sectional geometry, or loading

rate. Recalibration of the material model's non-physical parameters did yield accurate predictions for certain cases, although this is evidence that the material model lacks robustness, which is typical of other material models.

Toward achieving the third objective, $[\pm 45/0_2]_s$ and $[0/\pm 45/90]_s$ UD-NCF carbon fibre/epoxy adhesively bonded double-hat channels were manufactured. The channels were sanded, cured with the adhesive, and then trimmed to size. Finally, 45° chamfers were machined as triggers. After machining additional fixtures, the channels were tested under axial compression at dynamic and quasi-static rates. Previous simulation models were then expanded to include the use of cohesive elements and tiebreak contacts to represent the adhesive bond. The calibration of cohesive models based on experimental characterization of the adhesive was completed via additional simulation models of double cantilever beam (DCB) and end-notched flexure (ENF) tests. The simulation model results were compared to average experimental data as well as single-channel counterparts subject to the same experimental conditions. The following conclusions were noted:

- For all double hat channels tested, cohesive failure was not observed. Instead, delamination between plies adjacent to the bond was observed.
- In terms of stacking sequence, the stiffer channels absorbed more energy under both dynamic and quasi-static axial loading, due to the increased number of plies oriented in the direction of loading. However, at quasi-static rates, the difference in energy absorption between the stacking sequences was only 7%, while at dynamic rates, the difference in energy absorption was 31%. This was attributed to the similarity in crack propagation mechanisms demonstrated at quasi-static rates.
- In terms of loading rate, reduced energy absorption was exhibited by channels tested at dynamic rates, regardless of stacking sequence, in comparison to testing at quasi-static rates. Additionally, higher peak forces were measured with the dynamic tests than the quasi-static tests. While some extent of brittle fracture was observed for all tested channels, fragmentation was predominant at dynamic rates, while splaying was predominant at quasi-static rates. The latter failure modes, which facilitate increased friction and new surfaces, resulted in increased energy absorption.
- The failure modes observed for the double-hat channels were distinct from those observed for the open channels in some cases. Most notably, between the $[\pm 45/0_2]_s$

channels, where less fragmentation and larger frond formation was observed in the double-hat channels dynamically. Additionally, in comparison to the single-hat channels, all double-hat channels exhibited a decrease in specific energy absorption (between 7% and 33%). This was expected to be because the bonding of the channels resulted in less frond formation of the plies adjacent to the adhesive. Additionally, defects may have been introduced during the sample preparation of the double-hat channels.

- MAT_240 was successfully calibrated to replicate the experimentally measured Mode I response of 3M IRSA 07333. However, accurate calibration to the experimentally measured Mode II (shear) response via ENF tests was not achieved. This influenced the simulation model results of the double-hat channels, which predicted premature failure of the cohesive elements as a result of shear forces.
- The single shell representation of the hat channels could not accurately represent the stiffness of the flanges and contributed to the prediction of Mode II cohesive failure via non-physical flange deformations.

7.2 Recommendations for Future Work

While progress was made in understanding the failure behaviour of UD-NCF carbon fibre/epoxy channels, additional work is required to support the development of a high-fidelity simulation model. This includes the following recommendations:

Experimental Characterization of Single UD-NCF Channels

- Additional microscopy evaluating the microstructure of channels with complex geometries per stacking sequence would provide insight into causes for reduced or improved energy-absorbing capacity.
- Additional dynamic axial loading tests to identify the causes of changes in macroscopic failure modes would provide insight into design decisions for crash-worthy composite channels.

- Dynamic axial loading tests with different stacking sequences and geometries subject to oblique impact crush loading would provide insight into the EAC of UD-NCF CFRPs for a case that is representative of frontal off-axis impact conditions for a vehicle.
- Dynamic axial loading tests of channels with various trigger geometries to understand the influence of the trigger on the peak force would provide insight into maximizing crush force efficiencies.

Simulation Model Development

- The channels in the simulation models should be recreated with multi-layer shells connected via tiebreak contacts to represent the plies. This would be in an attempt to capture the effect of delamination on the energy-absorbing capacity of the channels and the predicted macroscopic failure modes. As opposed to representing all plies, studies using three layers of shells (two halves and a debris wedge) could also be considered to maximize computational efficiency.
- Alternative numerical representations of the trigger, which can better capture the shape of the trigger or the stacking sequence at the trigger, should be considered. While the trigger does not influence most of the total energy absorption, it does distinguish between progressive crushing and other non-desirable responses. Additionally, predictions of unrealistically high peak forces correspond to underpredicted mean crush forces.
- A material model which accounts for the strain-rate dependency of the composite constituents is necessary to account for the effect of loading rate on the microscopic failure modes observed in the channels. Additionally, a reduction in the reliance on non-physical calibration would improve the predictive capability of the material model.
- Non-local damage and failure options in LS-DYNA should be investigated to aid in reducing the mesh size sensitivity on material softening. The associated data smoothing over a set of neighboring elements may improve damage onset and evolution predictions.
- The development of simulation models which implement Lagrangian to Smooth Particle Hydrodynamics (SPH) formulation may better capture the failure of the channels.

- Lastly, information from a forming simulation that could capture defects or misalignment introduced as a result of complex geometries would also improve the predictive capability of an advanced model.

Experimental Characterization and Numerical Simulation Model Development of Double-Hat Channels

- Experimentally, testing additional bonded channels under axial loading and three-point bending to provide additional insight into their energy-absorbing capacity, as well as verify the repeatability of these results, is required. Alternative trigger geometries should be tested to reduce the peak forces under dynamic loading and improve the crush force efficiency.
- Many of the suggestions for the single-channel simulation models apply to the double-hat channel simulation models as well. Beyond this, a reduction in the element size of the cohesive elements, increased adhesive material model calibration, or the consideration of alternative contact cards could improve the predictive capability associated with the contribution of the bond line. Additionally, further calibration MAT_240 to capture the response of the adhesive under Mode II loading is required.

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Figure	Right Holder	Reference	License Number	License Date
1.2	Elsevier	A. Jacob, “BMW counts on carbon fibre for its Megacity Vehicle,” <i>Reinforced Plastics</i> , vol. 54, no. 5, pp. 38–41, Sep. 2010, doi: 10.1016/S0034-3617(10)70173-9.	59932309 00739	20 Mar. 2025
2.1	JLON Composite	JLON Composite, “How To Make Carbon Fibre Fabric?,” JLON. [Online]. Available: https://www.jloncomposite.com/how-to-make-carbon-fibre-fabric.html	By email.	21 Mar. 2025
2.2	Elsevier	F. J. Schirmaier, K. A. Weidenmann, L. Kärger, and F. Henning, “Characterisation of the draping behaviour of unidirectional non-crimp fabrics (UD-NCF),” <i>Composites Part A: Applied Science and Manufacturing</i> , vol. 80, pp. 28–38, Jan. 2016, doi: 10.1016/j.compositesa.2015.10.004.	59939709 12178	21 Mar. 2025
2.3	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, <i>Analysis and performance of fibre composites</i> , 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.5	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, <i>Analysis and performance of fibre composites</i> , 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.6	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, <i>Analysis and performance of fibre composites</i> , 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.7	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, <i>Analysis and performance of fibre composites</i> , 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.8	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, <i>Analysis and performance of</i>	1551440-1	30 Nov. 2024

		fibre composites, 3rd ed. Hoboken, N.J: John Wiley, 2006.		
2.9	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, Analysis and performance of fibre composites, 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.10	Elsevier	B. D. Agarwal, L. J. Broutman, and K. Chandrashekhara, Analysis and performance of fibre composites, 3rd ed. Hoboken, N.J: John Wiley, 2006.	1551440-1	30 Nov. 2024
2.11	Springer Nature	C. A. Mota Soares, <i>Mechanics of Composite Materials and Structures</i> . in Nato Science Series E: Ser, no. v. 361. Dordrecht: Springer Netherlands, 1999.	60332102 40938	20 May 2025
2.12	Diamond Scientific Publication	Ö. Özbek, “The Influence of Fibre Orientation on Crashworthiness Behaviour of Carbon Fibre Reinforced Composite Pipes,” <i>Eur. J. Eng. Sci. Technol.</i> , Sep. 2019, doi: 10.33422/EJEST.2019.08.16.	By email	20 May 2025
2.13	Sage	G. L. Farley and R. M. Jones, “Crushing Characteristics of Continuous Fibre-Reinforced Composite Tubes,” <i>J. Compos. Mater.</i> , vol. 26, no. 1, pp. 37–50, Jan. 1992, doi: 10.1177/002199839202600103.	60331900 87623	20 May 2025
2.14	Elsevier	X. Zhao, G. Zhu, C. Zhou, and Q. Yu, “Crashworthiness analysis and design of composite tapered tubes under multiple load cases,” <i>Composite Structures</i> , vol. 222, p. 110920, Aug. 2019, doi: 10.1016/j.compstruct.2019.110920.	59189808 07247	30 Nov. 2024
2.15	Elsevier	X. Zhao, G. Zhu, C. Zhou, and Q. Yu, “Crashworthiness analysis and design of composite tapered tubes under multiple load cases,” <i>Composite Structures</i> , vol. 222, p. 110920, Aug. 2019, doi: 10.1016/j.compstruct.2019.110920.	59189808 07247	30 Nov. 2024
2.17	Elsevier	D. Siromani et al., “An experimental study on the effect of failure trigger mechanisms on the energy absorption capability of CFRP tubes under axial compression,” <i>Composites Part A: Applied Science and Manufacturing</i> , vol. 64, pp. 25–35, Sep. 2014, doi: 10.1016/j.compositesa.2014.04.019.	59189811 10961	30 Nov. 2024
2.18	Elsevier	S. Boria, A. Scattina, and G. Belingardi, “Axial energy absorption of CFRP truncated cones,”	60136001 69352	21 Apr. 2025

		<i>Compos. Struct.</i> , vol. 130, pp. 18–28, Oct. 2015, doi: 10.1016/j.compstruct.2015.04.026.		
2.20	Elsevier	Q. Liu, H. Xing, Y. Ju, Z. Ou, and Q. Li, “Quasi-static axial crushing and transverse bending of double hat shaped CFRP tubes,” <i>Compos. Struct.</i> , vol. 117, pp. 1–11, Nov. 2014, doi: 10.1016/j.compstruct.2014.06.024	59189814 30746	30 Nov. 2024
2.21	Elsevier	Q. Liu, H. Xing, Y. Ju, Z. Ou, and Q. Li, “Quasi-static axial crushing and transverse bending of double hat shaped CFRP tubes,” <i>Compos. Struct.</i> , vol. 117, pp. 1–11, Nov. 2014, doi: 10.1016/j.compstruct.2014.06.024	59189814 30746	30 Nov. 2024
2.22	Elsevier	D. Chen, G. Sun, X. Jin, and Q. Li, “Quasi-static bending and transverse crushing behaviours for hat-shaped composite tubes made of CFRP, GFRP and their hybrid structures,” <i>Compos. Struct.</i> , vol. 239, p. 111842, May 2020, doi: 10.1016/j.compstruct.2019.111842.	60332002 16961	20 May 2025
2.24	Elsevier	B. Liang <i>et al.</i> , “Multi-scale modeling of mechanical behaviour of cured woven textile composites accounting for the influence of yarn angle variation,” <i>Compos. Part Appl. Sci. Manuf.</i> , vol. 124, p. 105460, Sep. 2019, doi: 10.1016/j.compositesa.2019.05.028	60332006 70164	20 May 2025

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Appendix A

History Variables for MAT_054

Information about each layer/integration point defined within PART_COMPOSITE can be plotted using additional integration point variables (NEIPS in *DATABASE_BINARY for shells). The additional variables are tabulated below (Table A.1), where i is an additional shell integration point.

Table A.1: History variables available in LS-DYNA for the material model, MAT_054 [158].

History Variable	Description	Value
1 - $ef(i)$	Tensile fibre mode	1: Elastic 0: Failed
2 - $ec(i)$	Compressive fibre mode	
3 - $em(i)$	Tensile matrix mode	
4 - $ed(i)$	Compressive matrix mode	
5 - $efail(i)$	Max [$ef(ip)$]	
6 - $dam(i)$	Damage parameter (SOFT)	-1: element intact 10 ⁻⁸ : element in crash front +1: element failed
8- $q1$	Material direction cosine (alpha)	Alpha: in-plane angle between material coordinate system and element coordinate system.
9 - $q2$	Material direction sine (alpha)	
10- ε_{aa}	Local strain a-direction	
11- ε_{bb}	Local strain b-direction	
12- ε_{ab}	Local shear strain (ab-plane)	
16 - d_{56}	Transverse shear damage	0: intact 1: damaged

Appendix B

ZOLTEK™ PX35 Uni-Directional Fabrics Technical Datasheets

Technical Datasheet

ZOLTEK™ PX35 Uni-Directional Fabrics



Stitch-Bonded Uni-Directional Carbon Fabrics

DESCRIPTION

ZOLTEK PX35 Stitch-Bonded Uni-Directional Carbon Fabrics are produced from our ZOLTEK PX35 50K Continuous Tow Carbon Fiber. Unique fiber spreading techniques are utilized to obtain a wide range of UD fabric weights for a varied set of composite part applications. Quick composite part build-up is cost effectively achieved with our diverse weight range of low-cost carbon fabric products.



MATERIAL OVERVIEW	UD150	UD200	UD300	UD400	UD500	UD600	UD900V
0° Carbon ZOLTEK PX35 50K	158	200	309	403	500	600	865
90° Glass 34 dtex	10	10	10	10	10	10	—
Polyester Veil	—	—	—	—	—	—	30
Polyester Stitch 76 dtex	6	6	6	6	6	6	5
Total Fabric Weight	182 g/m ² 5.37 oz/yd ²	224 g/m ² 6.61 oz/yd ²	333 g/m ² 9.82 oz/yd ²	419 g/m ² 12.36 oz/yd ²	516 g/m ² 15.22 oz/yd ²	624 g/m ² 18.40 oz/yd ²	900 g/m ² 26.54 oz/yd ²

Average Values Shown
*Epoxy resin binder available upon customer request.

FABRIC CONSTRUCTION	UD150	UD200	UD300	UD400	UD500	UD600	UD900V
Stitch Length	A variety of stitch lengths are available to meet application requirements.						
Stitch Pattern	A variety of stitch patterns are available to meet application requirements.						
Cured Thickness/Ply	.21 mm	.25 mm	.37 mm	.46 mm	.57 mm	.69 mm	1.00 mm
Roll Width	30 cm - 61 cm - 122 cm						122 cm
Roll Length	100 m				50 m		30 m

Average Values Shown

The properties listed in this datasheet do not constitute any warranty or guarantee of values. This information should only be used for the purposes of material selection. Please contact us for more details.

ZOLTEK™ PX35



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Technical Datasheet

ZOLTEK™ PX35 Uni-Directional Fabrics



Stitch-Bonded Uni-Directional Carbon Fabrics

COMPOSITE PROPERTIES	SI	US	METHOD
Tensile Strength	1,400 MPa	203 ksi	DIN EN ISO 527
Tensile Modulus	119 GPa	17.2 msi	DIN EN ISO 527
Compressive Strength	980 MPa	142 ksi	DIN EN ISO 14126
Compressive Modulus	118 GPa	17.5 msi	DIN EN ISO 14126
Flexural Strength	1,290 MPa	187 ksi	DIN EN ISO 14125
Flexural Modulus	112 GPa	16.2 msi	DIN EN ISO 14125

Typical Fiber Volume Fraction (FVF) is 55%.
Standard Epoxy Resin System

The properties listed in this datasheet do not constitute any warranty or guarantee of values. This information should only be used for the purposes of material selection. Please contact us for more details.

TYPICAL PACKAGING

Wound on cardboard cone, sealed in polyethylene bag, and placed in cardboard box. Rolls stacked horizontally on pallets when shipping.

+ Requirements other than standard widths and roll lengths should be specified by purchase order.

CERTIFICATION

ZOLTEK PX35 Fabrics are manufactured in accordance with ZOLTEK's written and published data. A Certificate of Conformance is provided with each shipment.

SAFETY

Obtain, read, and understand the Material Safety Data Sheet (SDS) before use of this or any other ZOLTEK product.

APPROVAL

DNV-GL has granted approval to ZOLTEK PX35 Uni-Directional Fabrics for use in wind energy applications.



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TECHNICAL DATASHEET

ZOLTEK™ PX35 CONTINUOUS TOW

DESCRIPTION

ZOLTEK PX35 continuous carbon fiber is manufactured from polyacrylonitrile (PAN) precursor. The consistency in yield and mechanical properties that are provided by large filament count strands gives the user the ability to design and manufacture composite materials with greater confidence and allows for efficient and fast buildup of carbon fiber reinforced composite structures.

ZOLTEK PX35 50K fibers are available with a variety of sizing formats for different composite processing methods and for compatibility with a wide range of standard resin systems. ZOLTEK quality focuses on spool to spool consistency yielding low coefficients of variation.

APPLICATIONS

Wind energy, automotive, petroleum production, aviation, marine, industrial, other transportation, medical (including X-ray), sports and recreation, etc.

RECOMMENDED USE

Any article or component benefiting from the unique properties of carbon fiber composites.



MATERIAL OVERVIEW	SI	US
Tensile Strength	4,137 MPa	600 ksi
Tensile Modulus	242 GPa	35 msi
Elongation	1.5%	1.5%
Electrical Resistivity	0.00155 ohm-cm	0.00061 ohm-in
Density	1.81 g/cc	0.065 lb/in ³
Fiber Diameter	7.2 microns	0.283 mils
Carbon Content	95%	95%
Yield	267 m/kg	397 ft/lb
Textile Units	267 m/kg	33700 denier
Spool Weight	5.5 kg, 11 kg	12 lb, 24 lb
Spool Length	1,500 m , 3,000 m	1,640 yd, 3,280 yd

The properties listed in this datasheet do not constitute any warranty or guarantee of values. This information should only be used for the purposes of material selection. Please contact us for more details.



TECHNICAL DATASHEET

ZOLTEK™ PX35 CONTINUOUS TOW

TYPICAL PACKAGING

Wound on a 3" x 11" (7.6 cm x 28 cm) cardboard spool, sealed in heat shrunk covering, and placed in cardboard box; two splices are permitted per every 1,500m spool and five per every 3,000m spool.

Spool outside diameter:

- 1,500 m: 180-185 mm
- 3,000 m: 235-245 mm

APPROVAL

DNV-GL has granted approval to ZOLTEK PX35 Continuous Tow for use in wind energy applications.

CERTIFICATION

ZOLTEK PX35 Continuous Tow is manufactured in accordance with ZOLTEK's written and published data. A Certificate of Conformance is provided with each shipment.

SAFETY

Obtain, read, and understand the Material Safety Data Sheet (SDS) before use of this or any other ZOLTEK product.



Appendix C

Drawings of Fixtures Used for Experimental Tests

The dimensions of the fixtures manufactured for the experimental axial loading of the tapered hat and double-hat channels are presented below.

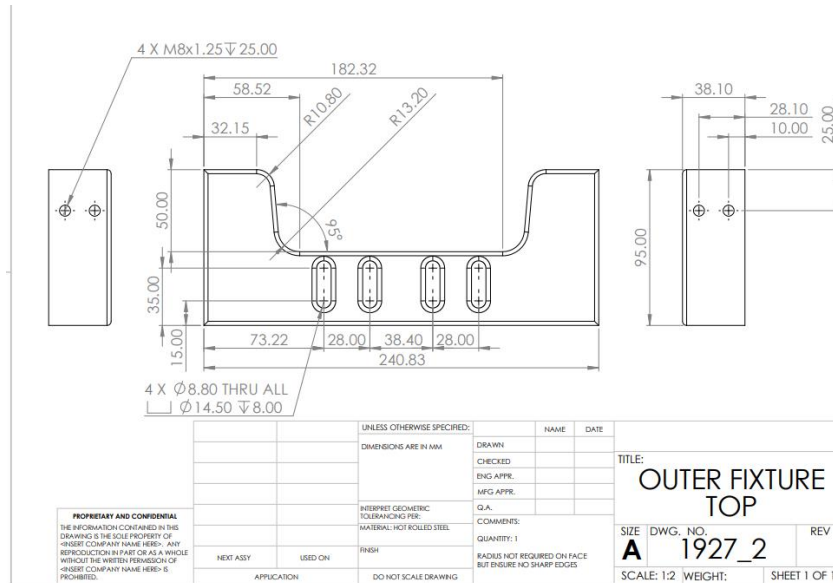


Figure C.1: Drawing of fixture manufactured for the outside profile of tapered hat channel [159].

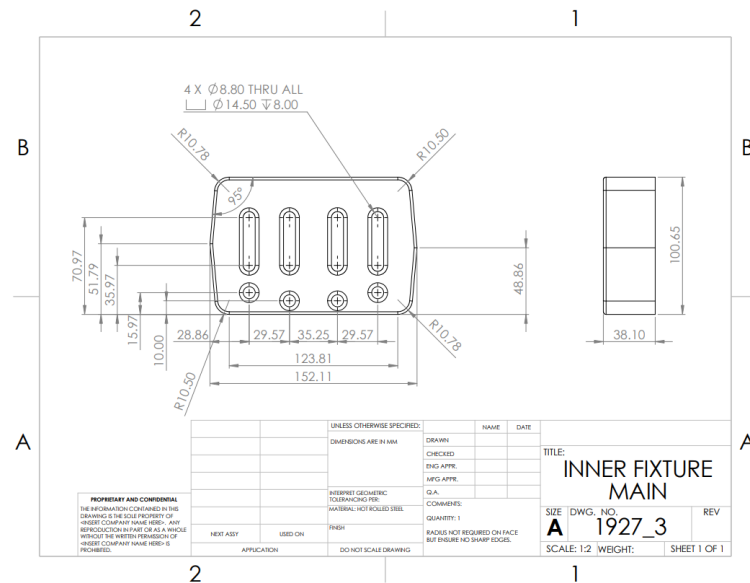


Figure C.2: Drawing of fixture manufactured for the inside profile of tapered hat channel [159].

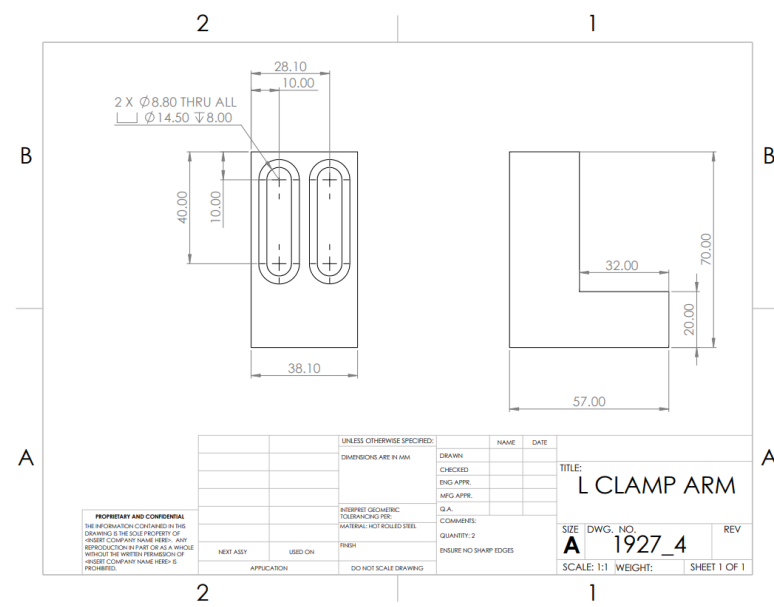


Figure C.3: Drawing of fixtures manufactured to clamp the inside and outside profile fixtures of the tapered hat channel together [159].

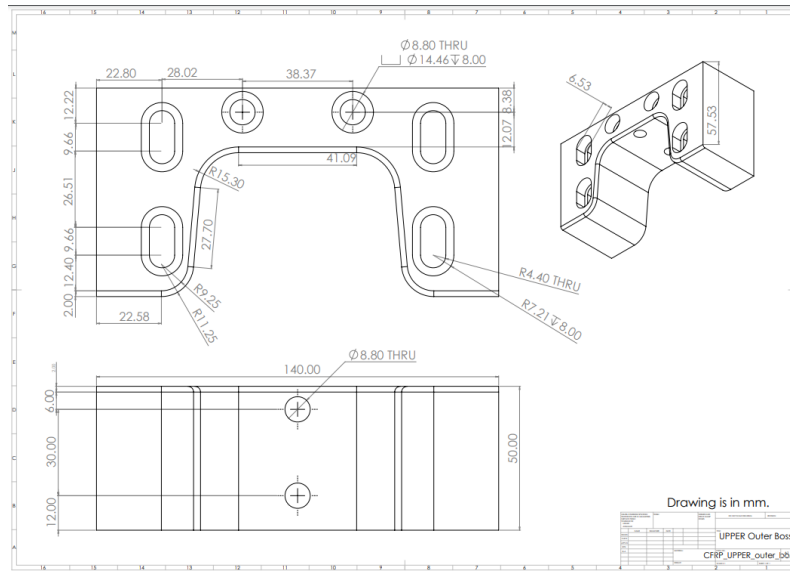


Figure C.4: Drawing of fixture manufactured for the top half of double-hat channels.

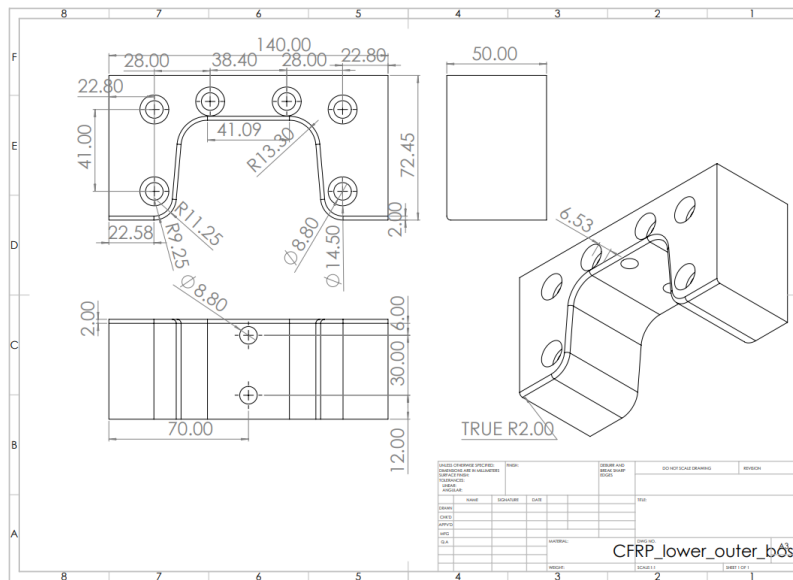


Figure C.5: Drawing of fixture manufactured for the bottom half of double-hat channels [25].

Appendix D

Mesh Geometry Calibration & Convergence Studies

Initially, the simulation models were developed using an angled quadrilateral mesh as suggested by Dharmaraj [37], who calibrated MAT_054 for the same baseline condition. Additionally, Dharmaraj noted that after testing meshes between two millimetres to six millimetres, five millimetres was found to be an appropriate element size for balancing computational efficiency and result accuracy. Since the exact dimensions of the angled elements were not provided by Dharmaraj (Dharmaraj, 2020), a similar mesh was recreated, and a new mesh convergence study for element sizes of two to six millimetres was completed (Figure D.1). To complete the study, the recommended initial values as per the literature, and the default values provided by LSTC were provided for MAT_054 (Table 4.2).

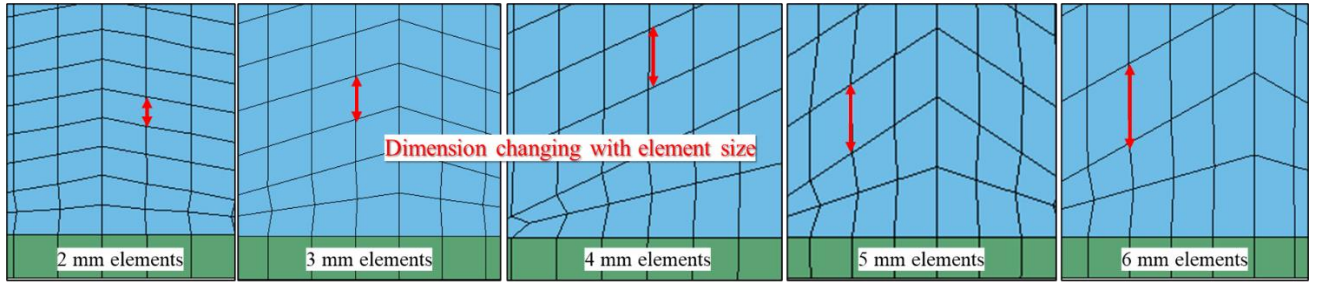


Figure D.1: Angled mesh geometries for element sizes of two to six millimetres.

Initially, failure strains were inputted to MAT_054 based on available experimental results [140] and the hat channel part was created with an element size of five millimetres. However, the performance of the channel (i.e. force-displacement response) was significantly underpredicted. Wade and Feraboli (Wade & Feraboli, 2016) proposed that it was possible to replace the strain values with EFS in accordance with the following equation:

$$EFS = \sqrt{\frac{4}{3}(\varepsilon_{11}^2 + \varepsilon_{11}\varepsilon_{12} + \varepsilon_{22}^2 + \varepsilon_{12}^2)} \quad (1)$$

Where ε_{11} , ε_{22} , ε_{12} corresponded to the strains in the 1-, 2- and 12- direction at element failure. With the experimentally derived values [140], EFS was determined to be 0.11. While this improved the force-displacement response, it still resulted in premature fracture. As a result, an arbitrary increase in EFS was selected and subsequent calibration proceeded as described in Chapter 4 and Chapter 6. Next, a mesh convergence study was completed (Figure D.2) for which stable crush forces were

demonstrated at all element sizes. Since the simulation model was calibrated to an element size of five millimetres, this provided the most accurate predictions in comparison to the simulation model results with the hat channel meshed at the other element sizes. Changes in element sizes altered the period and magnitude of the unfiltered force-displacement results and thus were distinctly affected by subsequent filtering. Additionally, the time step element deletion criteria in MAT_054 may cause the premature deletion of smaller elements which deformed greatly during loading.

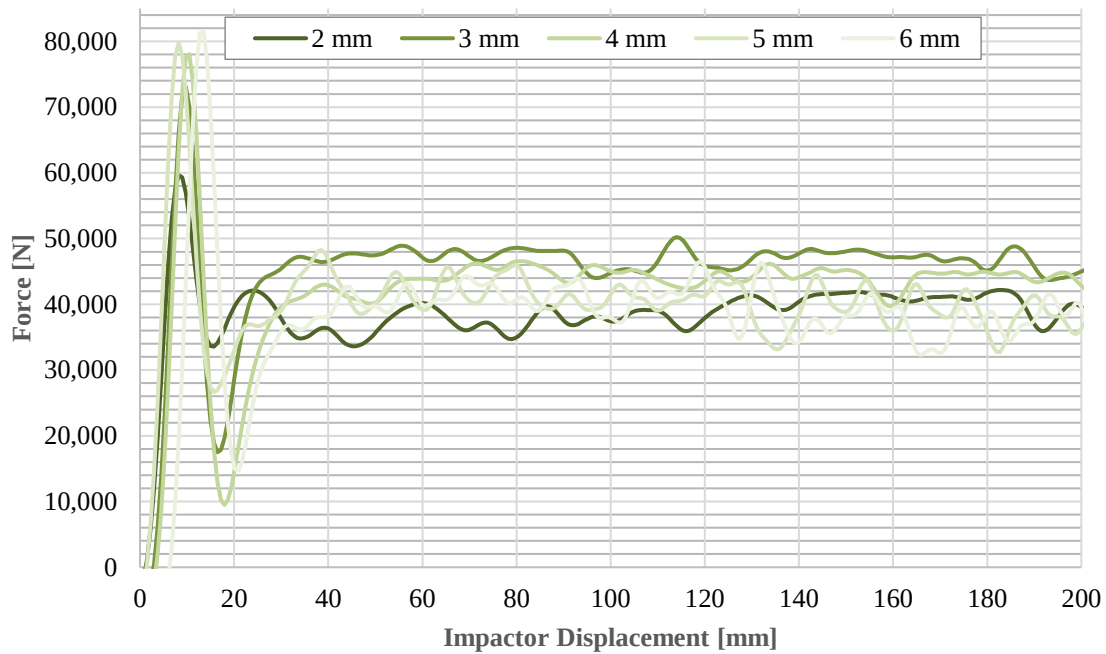


Figure D.2: Influence of (angled quadrilateral) element size on force-displacement response of $[0/\pm 45/90]_s$ hat channel subject to dynamic axial loading.

With the completed simulation model for the baseline condition using angled quadrilateral mesh geometry, the channel geometry was updated to model corrugated and tapered channels. The force-displacement responses (Figure D.3 and Figure D.4) demonstrated good agreement as well. However, when the $[\pm 45/0_2]_s$ hat channel was modelled, significant underprediction for the mean crush force was observed (Figure D.5).

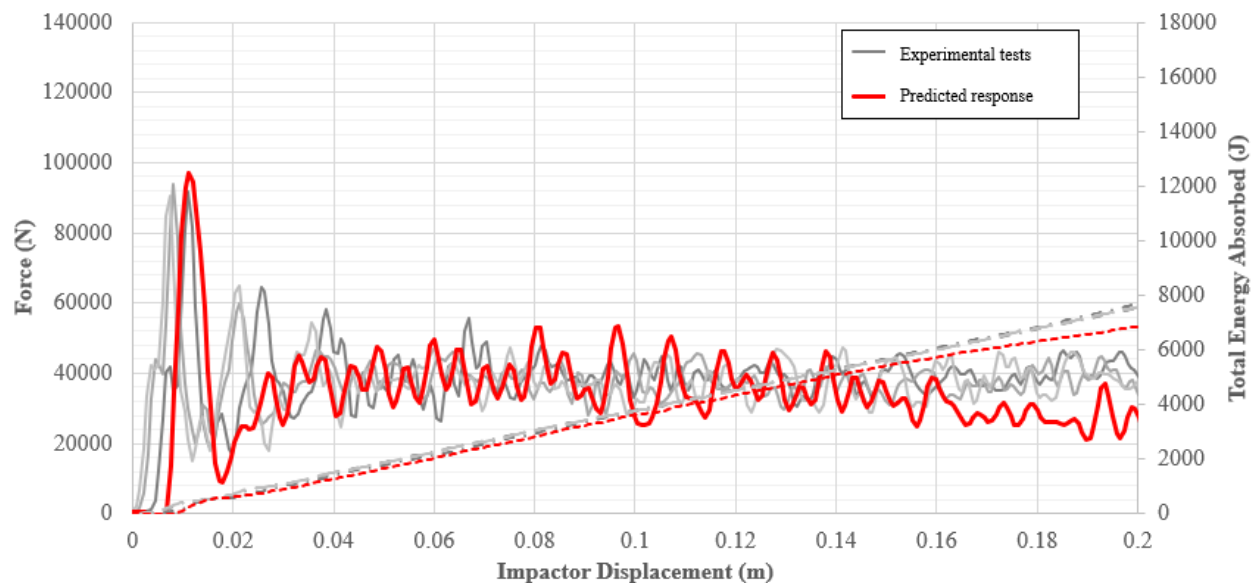


Figure D.3: Force-displacement response of the simulation model for the dynamic axial loading of the $[0/\pm 45/90]_s$ tapered hat channel with an angled quadrilateral mesh applied for the channel part representation.

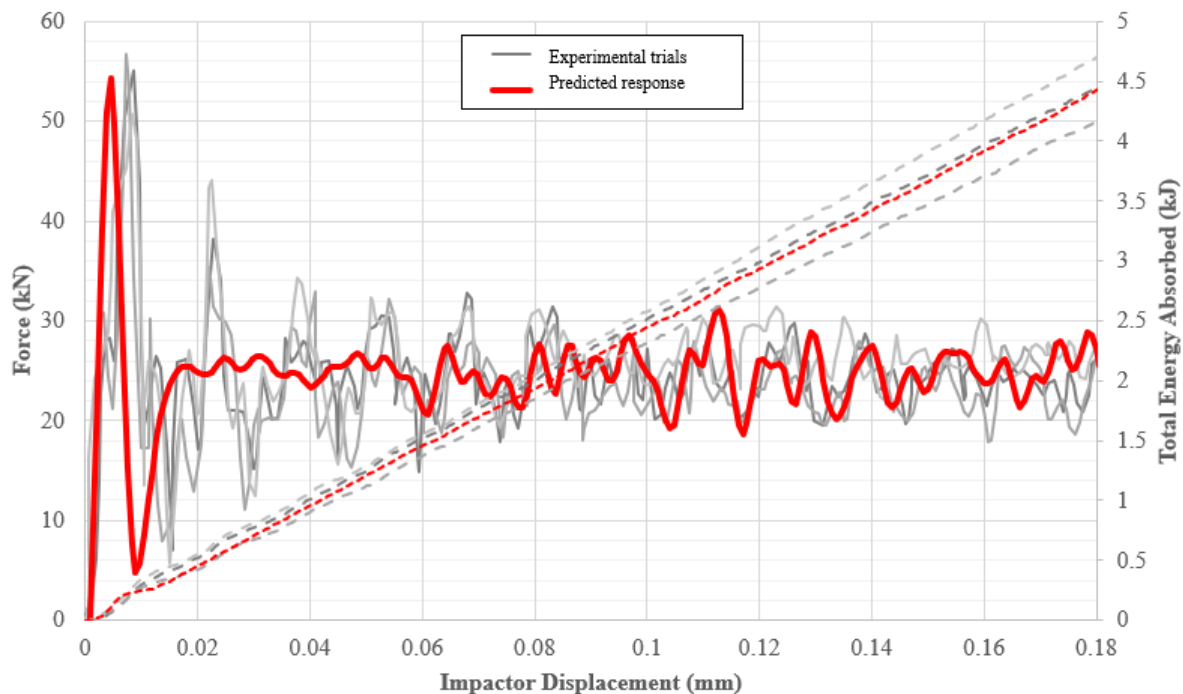


Figure D.4: Force-displacement response of the simulation model for the dynamic axial loading of the $[0/\pm 45/90]_s$ corrugated channel with an angled quadrilateral mesh applied for the channel part representation.

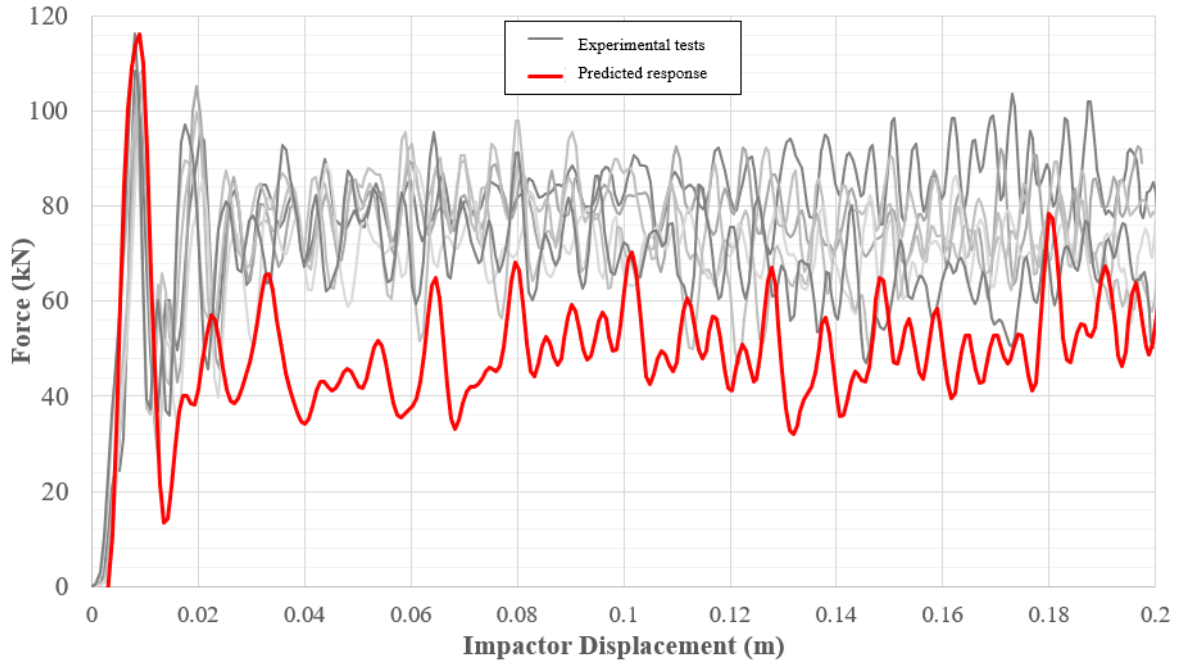


Figure D.5: Force-displacement response of the simulation model for the dynamic axial loading of the $[\pm 45/0_2]_s$ hat channel with an angled quadrilateral mesh applied for the channel part representation.

The simulation model created for the baseline condition using the angled quadrilateral mesh above was then updated to replace the hat channel with triangular and quadrilateral elements. The results, while maintaining the same calibrated values and loading conditions, demonstrated the influence of mesh structure (Figure D.6). Namely, the structured quadrilateral exhibited increased instability for the same calibrated conditions in comparison to the unstructured triangular and angled quadrilateral mesh. This is analogous to the catastrophic failure often predicted when the softening factors included in MAT_054 for modelling CFRP materials are not used. The non-uniform crush front provided by the unstructured meshes aids in avoiding predictions of catastrophic failure.

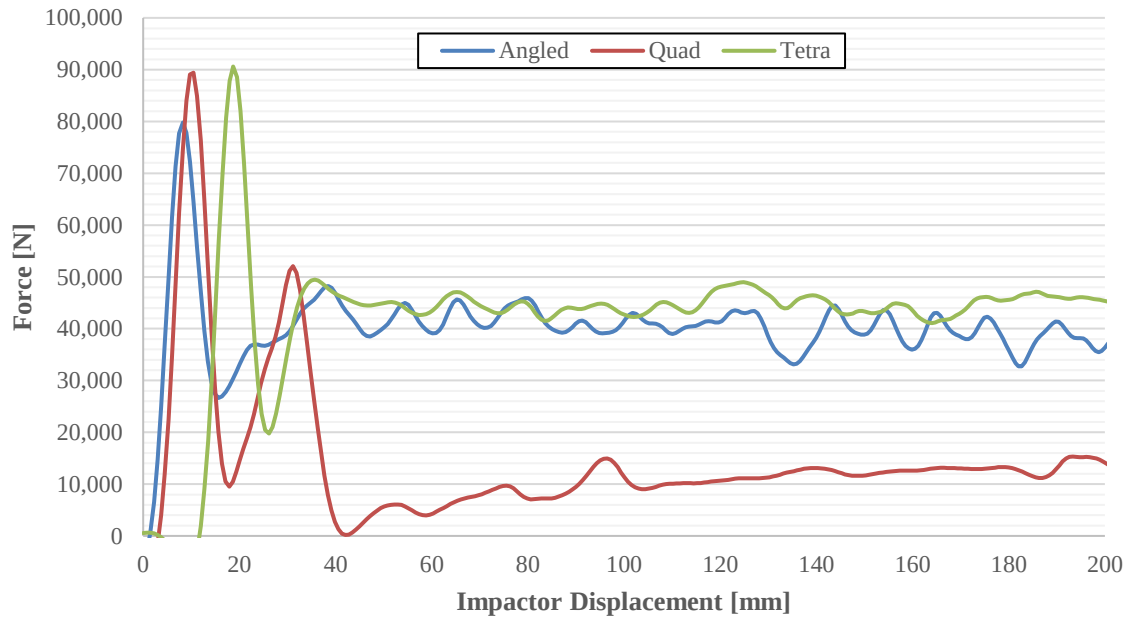


Figure D.6: Influence of element mesh geometry on the force-displacement simulation model results of a $[0/\pm 45/90]_s$ hat channel subject to dynamic axial loading.

As discussed in Chapter 4, it was thus necessary to recalibrate the simulation model for the baseline condition with quadrilateral elements representing the hat channel. A new mesh convergence study was then completed with this element mesh structure (Figure D.7). At lower element sizes, the channel fails prematurely, likely as a result of the element time step deletion criteria included in MAT_054 and determined based on the time step size of a five-millimetre element. At all element sizes of 5 millimetres and greater, the force-displacement responses are comparable and stable.

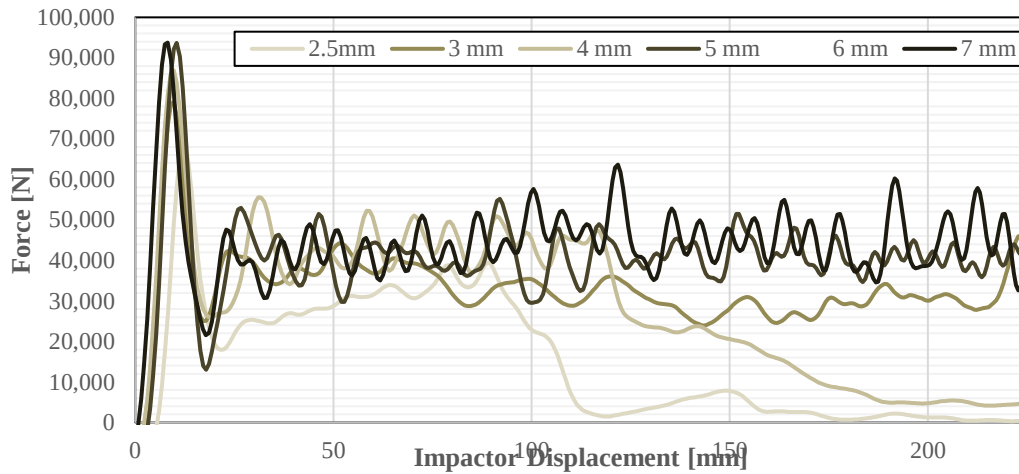


Figure D.7: Influence of (quadrilateral) element size on the force-displacement simulation model results of the $[0/\pm 45/90]_s$ hat channel subject to dynamic axial loading.

Lastly, an example of the force-displacement response outputted when using the quadrilateral mesh without filtering is presented below (purple, Figure D.8). A 300 Hz filter was applied on all outputted results for ease of comparison to the experimental data.

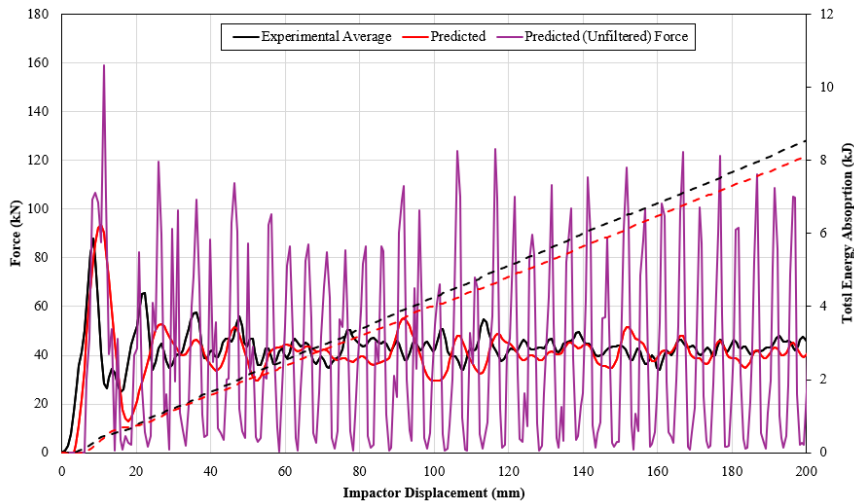


Figure D.8: The predicted (filtered), predicted (unfiltered), and experimental force-displacement responses of the dynamic axial loading of the $[0/\pm 45/90]_s$ hat channel with a quadrilateral mesh representation of the hat channel.

Appendix E

Calibration of Material Model for Impact Resistant Structural Adhesive 07333

To calibrate the Mode I and Mode II responses of the structural adhesive, IRSA 07333, experimental data from Dehaghani [120] and Gao [45] were used, respectively, for verification. Dehaghani [120] performed double-cantilever beam (DCB) tests on adhesively bonded UD-NCF carbon fibre/epoxy substrates, while Gao [45] performed end-notched flexure (ENF) tests. Corresponding simulation models for the DCB and ENF tests were created. MAT_138, a bilinear traction-separation law, and MAT_240, a trilinear traction-separation law, were considered for representing the adhesive. The former was selected based on numerical calibration completed by Dehaghani [120]. The latter was selected because trilinear laws are recommended for ductile adhesives, and calibrated parameters had been determined by Watson [142]. Additionally, representation of the adherends (UD-NCF CFRPs) was considered with both solid and shell elements. The former was considered because it allows the use of simpler contact algorithms, while the latter was considered because the simulation models developed in Chapter 4 employ shell layer representations of the hat channels. The development of DCB and ENF simulation models with all of these considerations (solid adherends and MAT138, solid adherends and MAT240, and shell adherends and MAT240) and their corresponding results are presented below.

Geometry and Mesh Generation

Half the width of the DCB sample was created to reduce computational time. As in the experimental setup, the adherends were 140 mm, and the thickness of each substrate was 2.27 mm (Figure E.1). The average pre-crack length for the samples of interest was 35.28 mm. For the simulation models comprising solid adherends, the adherends were meshed using fully integrated quadrilateral solids with a mesh size of 0.125 mm, 0.97 mm, and 0.5675 mm along the x-, y-, and z- directions, respectively, following the results of a mesh sensitivity study. The corresponding adhesive elements were meshed at a size of 0.125 mm, 0.97 mm, and 0.65 mm along the x-, y-, and z- directions, respectively. For the shell adherend representation (Figure E.2), the element size in the x and y directions was maintained, and a thickness of 2.27 mm was assigned to the shell. Type 16, fully integrated shell elements, were selected as the element formulation.

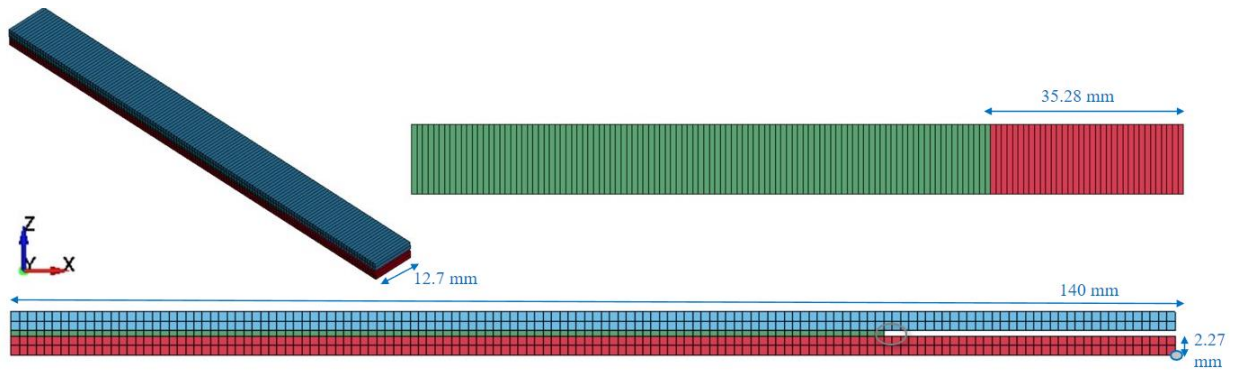


Figure E.1: Geometry and mesh created to replicate DCB experimental results with solid adherends.

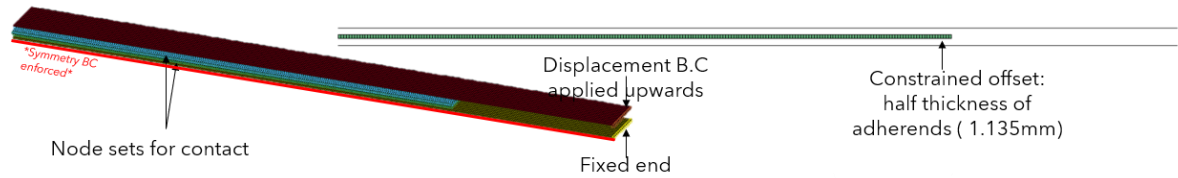


Figure E.2: Geometry and mesh created to replicate DCB experimental results with shell adherends.

The same geometry and mesh as the DCB samples was used for the ENF modelling with the exception of the adhesive thickness (Figure E.3). Since the available experimental results used a bond line thickness of 0.25mm, this change was reflected in the geometry and meshing. Since the DCB specimen was refined to final adherend dimensions of 0.25 mm (X) by 0.5675 mm (Z) by 0.97 mm (Y), these dimensions were the final size used for the ENF adherends. Once again, the adhesive was modelled as only one element thick in the z direction at 0.25 mm to replicate experimental conditions. The pre-crack length was approximately 30 mm. For the shell adherend representation (Figure E.4), the element size in the x and y directions are maintained and a thickness of 2.27 mm is assigned to the shell. The type 16, fully integrated shell element, was used for the parts.

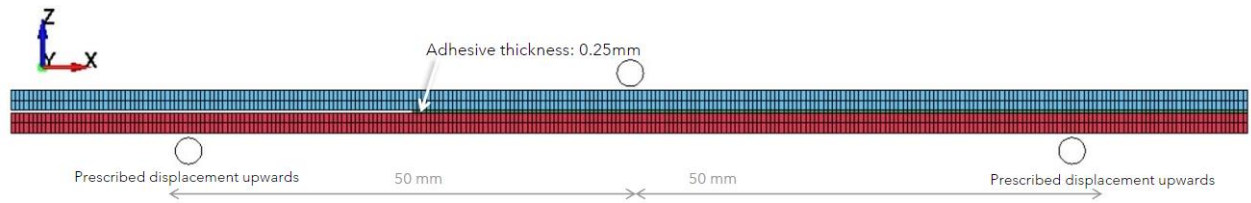


Figure E.3: Geometry and mesh created to replicate ENF experimental results with solid adherends.

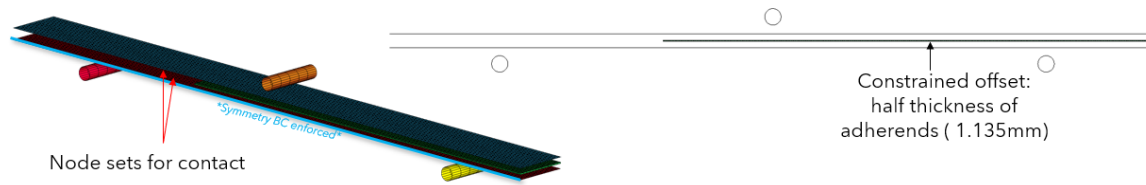


Figure E.4: Geometry and mesh created to replicate ENF experimental results with shell adherends.

Lastly, the fixtures are meshed as five rows of quadrilateral shell elements of size 0.47 mm by 2.8 mm for a total length of 14 mm each (Figure E.5).

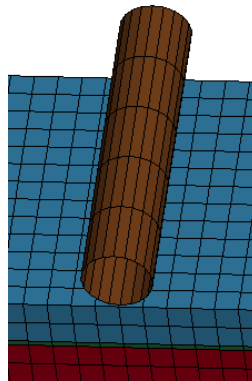


Figure E.5: Mesh and geometry of one of the fixtures created for the simulation models of DCB and ENF testing.

Material Models

Two materials are used for this model: one for the adherends and one for the adhesive. The adherends were defined as elastic orthotropic materials to capture the direction-dependent properties of the UD-NCF carbon fibre/epoxy material. The material properties were as follows [120]:

- Density: $1456 \frac{kg}{m^3}$
- Young Modulus in principal direction: 123.4 GPa
- Young's Modulus in tangential direction: 8.4 GPa
- Minor Poisson's ratio: 0.02186
- Major Poisson's ratio: 0.37
- Shear Modulus: 3.4 GPa

The adhesive is defined with a cohesive element specific material model (MAT138: cohesive mixed mode). This material model can take both the properties for Mode I and Mode II crack propagation. The properties for Mode I are as follows [120]:

- Density: $1320 \frac{kg}{m^3}$
- Young's Modulus in normal direction: 1.259 GPa
- Strain energy release rate for Mode I: $1949 \frac{J}{m^2}$
- Peak traction in normal direction: 48.72 MPa
- Ultimate displacement in normal direction: 0.08 mm

The Mode II properties for crack propagation are defined as follows for the cohesive zone material model [45]:

- Young's Modulus in tangential direction: 2.2 GPa
- Strain energy release rate for Mode II: $7000 \frac{J}{m^2}$
- Peak traction in tangential direction: 29 MPa
- Ultimate displacement in tangential direction: 0.2 mm

It was found that using the originally provided SERR value from Gao [45] led to a significant underprediction of initial results for the force-displacement response. Measuring crack propagation is especially challenging for an ENF test and so the values from Watson [142] were used to estimate the Mode II properties. However, good agreement between the experimental and numerical results could not be achieved and thus, the material model was changed from MAT138 to MAT240, a trilinear traction-separation law used by Watson [142] in their modelling. Mesh convergence studies were completed with this new material model applied to both the DCB and ENF simulation models. The additional properties provided for MAT240 were as follows [142]:

- FG1 (parameter to describe the trilinear shape of traction separation law in Mode I): 0.36
- FG2 (parameter to describe the trilinear shape of traction separation law in Mode II): 0.36

Lastly, the fixtures included in this model are defined as rigid steel bodies with the following properties:

- Density: $7830 \frac{kg}{m^3}$
- Young's Modulus: 215 GPa
- Poisson's ratio: 0.28

Boundary & Initial Conditions

For the simulation model of the DCB test, symmetry was used to reduce the model run time, and so the appropriate boundary conditions were applied to the parts to enforce symmetry. Additionally, the top edge of the top substrate was constrained in all directions except displacement and rotation in the Z direction. Meanwhile, the bottom edge of the bottom substrate was constrained in all six degrees of freedom. The top edge was also prescribed a displacement of 30 mm over 50 milliseconds. Thirty millimetres was chosen to correspond with the experimental force-displacement graph [120], while 50 seconds was arbitrarily chosen based on the termination time at which inertial effects were no longer observable. No contact was defined for these parts as they were never expected to come into contact with one another, with the exception of the adhesive. For contact between the cohesive zone elements and the substrates, the nodes of each substrate which overlap with a node of a cohesive element were merged. Additionally, the normal of the cohesive zone elements were set such that they point toward the through-thickness direction of the substrates. As a result, the cohesive elements were always in contact with the substrates until they failed and would extend/displace only in the through-thickness direction (Z; the direction of the applied load). For the shell adherend representation, an additional contact card was added (Tied shell edge to surface constrained offset) which constrained the adherends to remain 1.135 mm (half the adherend thickness) from the adhesive while being tied to the elements. The final loading rate was 0.15 mm/ms with this representation.

For the simulation model of the ENF test, symmetry conditions were also applied to reduce the run time. Additionally, the top fixture was fixed in all degrees of freedom while the bottom fixtures were each prescribed a displacement curve in the z-direction. The final displacement was always set to 6 mm in accordance with experimental results [45], while the rate of displacement was

changed in succeeding iterations as the loading rate was noted to influence the results. Lastly, because it was expected that the adherends would come into contact with one another during the model, an automatic, frictionless, contact was introduced for all parts in relation to one another. For the shell adherend representation, the Tied shell edge to surface constrained offset contact card was again used, and the final loading rate was 0.8 mm/ms.

Results

To begin, the mesh convergence study using the solid adherends' representation for the DCB test is presented (Figure E.6). The final simulation model used an element size of 0.25 mm with increased data outputs and was well aligned with experimental data determined by Dehaghani [120]. Next, the force-displacement response measured during the DCB (Figure E.7) and ENF (Figure E.8) tests are presented. For all simulation model versions, there was strong agreement between the experimental results and the simulation model results. However, with the ENF tests, while there was improved agreement with MAT240 (in that the shape of the response was better captured), a discrepancy between the experimental and predicted results existed, suggesting that further calibration was required.

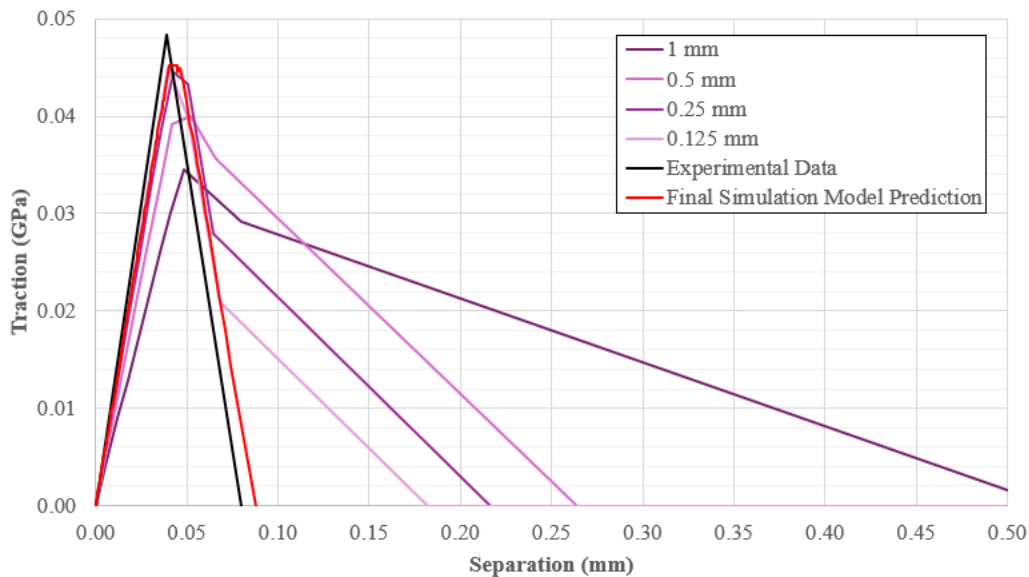


Figure E.6: Mesh convergence study for Mode I traction-separation response of cohesive elements representing the behaviour of structural adhesive, IRSA 07333. Experimental data from [120].

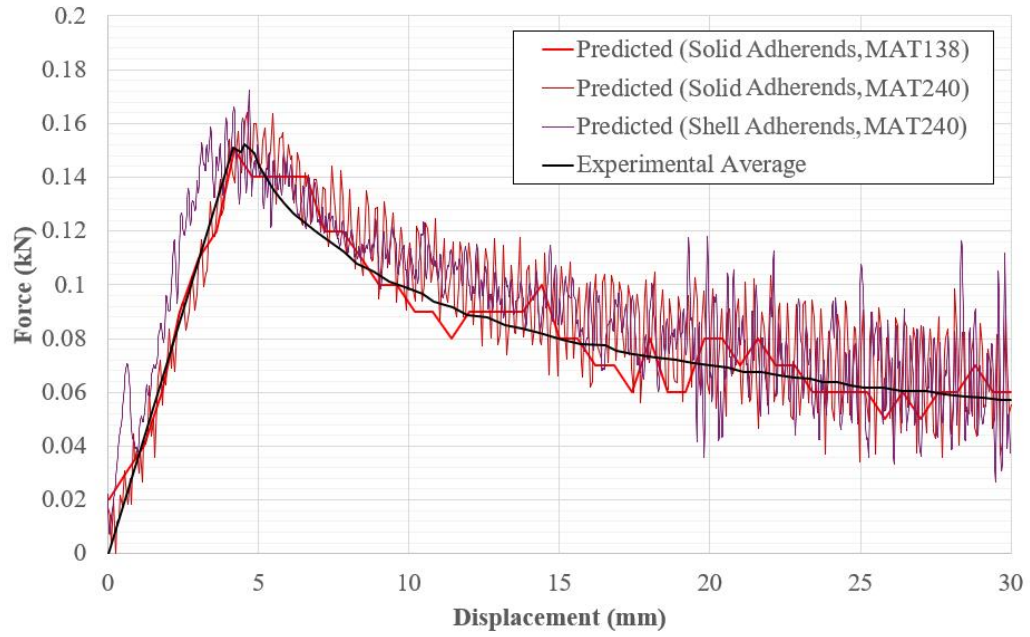


Figure E.7: Experimental [120] and predicted force-displacement responses for Mode I DCB test using MAT138 and MAT240 with solid and shell adherends.

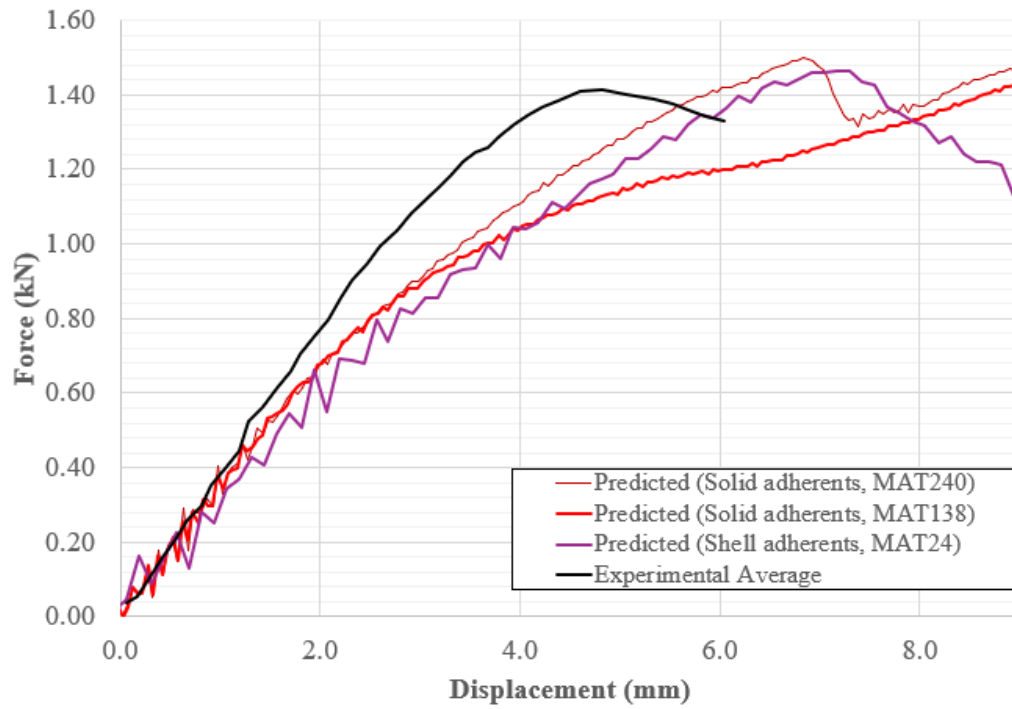


Figure E.8: Experimental [45] and predicted force-displacement responses for Mode II ENF test using MAT138 and MAT240 with solid and shell adherends.

Appendix F

Experimental Outliers

The force-time response of a $[0/\pm 45/90]_s$ tapered hat channel that buckled when subject to dynamic axial loading is presented (Figure F.1). At the onset of buckling, the mean crush force decreased significantly (near 0.1256 seconds). This was an unusual failure not observed with the other channels. It can be seen that the fracture of the final channel skewed towards one side in lieu of the other.

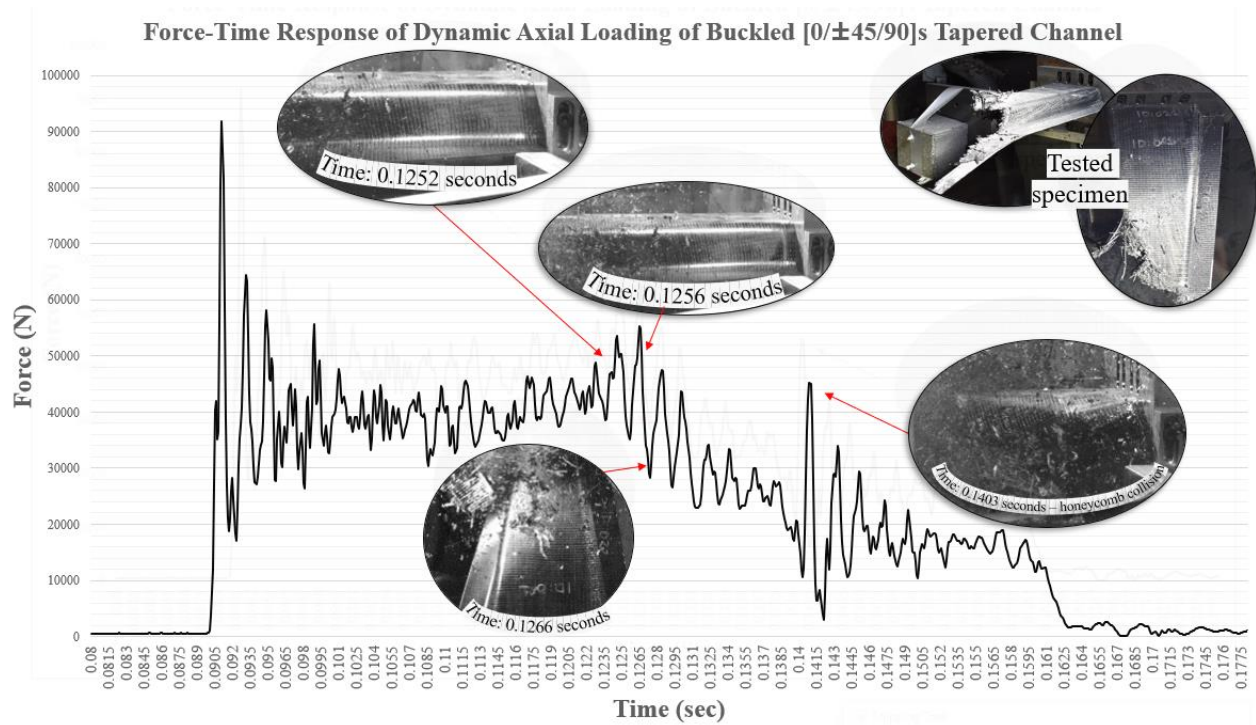


Figure F.1: Force-Time response of buckled $[0/\pm 45/90]_s$ tapered channel.

As mentioned in Chapter 3 and Chapter 5, the first double-hat channel tested (quasi-statically), debonded prematurely within the first 50 milliseconds of impact. This indicated that the bonding preparation method required adjustments and additional sanding to increase the surface roughness of the channels was introduced. However, as can be seen by the experimental force-displacement result associated with the testing (Figure F.2), the initial debonding did not significantly affect the energy absorbed during testing and progressive crushing was still achieved.

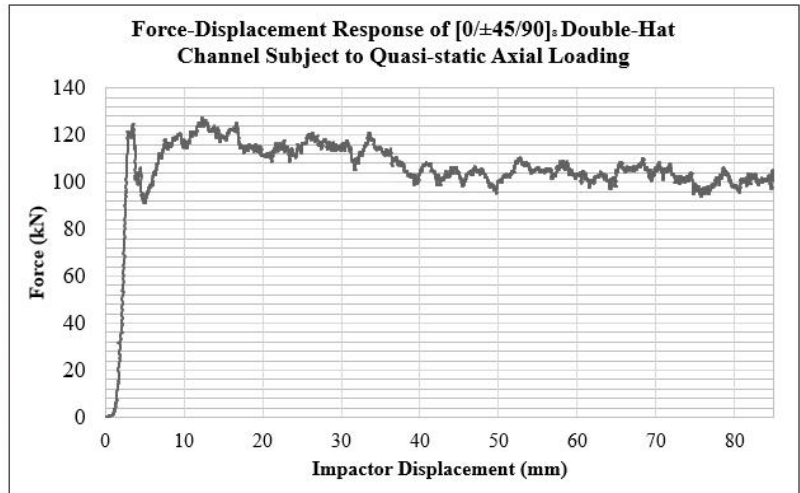


Figure F.2: Debonding of initial $[0/\pm 45/90]_s$ double-hat channel quasi-statically axially loaded.