



Through-the-Thickness Z-pinning Reinforcements to Improve Energy Absorption Capabilities of CFRP Crash Structures: Numerical Development

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Abstract

This study employs numerical methods to model through-the-thickness reinforcements in CFRP tubular structures under axial impact, investigating the influence of reinforcement configurations on crashworthiness performance. Experimental validation involves testing unpinned tubular structures to establish a baseline model. LS-DYNA finite element models simulate low-velocity axial impacts, incorporating energy-based tiebreak contacts or solid cohesive elements to describe interlaminar bridging. Through-the-thickness are introduced through a homogenous mesh system or locally refined mesh at pin locations. Various reinforced tube designs with different pin diameters and areal densities are examined to identify the optimal pinned design for crashworthiness. The research demonstrates numerically that pinning enhances crashworthiness performances in axial crushing of composite tubes.

Keywords Through-the-thickness · Composites · Crashworthiness · Cohesive · FEM

1 Introduction

Crashworthiness relies on quantifiable metrics such as Specific Energy Absorption (SEA), Mean Crushing Force (MCF), Crushing Force Efficiency (CFE), and Stroke Efficiency (SE). In composite tube crushing, failure modes like splaying and fragmentation involve energy absorption mechanisms, including fibre breakage and matrix cracking [1, 2]. A major challenge in composite applications is their susceptibility to delamination, prompting the development of through-thickness reinforcement techniques to enhance interlaminar fracture toughness. Among these, 3D weaving, stitching, braiding, and z-pinning have shown promise in improving delamination resistance and impact damage tolerance [3]. However, most techniques are restricted to textile laminates, as reinforcing prepreg laminates leads to fibre damage and degradation of in-plane mechanical properties [4]. Ghasemnejad et al. and

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Partridge et al. [5, 6] demonstrated the influence of interlaminar fracture toughness on SEA in axial crushing, with z-pinning proving effective in reducing delamination and improving toughness, albeit with minor reductions in in-plane properties. Lander [7] observed that z-pinned 8, 12, and 16-ply CFRP tubes exhibited 24%, 53%, and 76% SEA improvements, respectively, with rectangular z-pin patterns. The aerospace industry has explored z-pinned composites, with applications in damage-tolerant panels, bonded joints, and stiffened structures. The F/A-18E/F Super Hornet remains the only aircraft incorporating z-pins, replacing titanium fasteners in air inlet ducts and engine bay doors, enhancing structural integrity and damage tolerance [8]. Partridge et al. [9] reported that automating z-pin insertion can cut manufacturing costs by 70%, making it a competitive alternative to traditional fasteners. Despite these advantages, widespread adoption remains limited. Formula 1 roll-over bars represent the only known non-aerospace use of z-pinning [4]. Z-pinning involves inserting small-diameter pins or rods typically made of steel, titanium, or CFRP perpendicular to composite plies before curing [10]. Acting as local reinforcements, z-pins prevent delamination, enhance interlaminar shear strength, and introduce additional energy absorption mechanisms. However, z-pinning can reduce in-plane mechanical properties, with stiffness and strength decreasing linearly with pin size and content due to fibre crimping and waviness [11]. The trade-off between out-of-plane reinforcement and in-plane performance loss is crucial, as z-pin insertion creates microstructural flaws that weaken tensile and compressive properties [12]. Nevertheless, studies indicate that benefits outweigh drawbacks, with through-thickness modulus, impact resistance, and joint strength increasing by over 50% with a modest 2–4% z-pin volume fraction, while in-plane modulus and strength reductions remain under 5–10% [13]. Delamination toughness increases by nearly 500% with 2% z-pin content, highlighting its effectiveness in crack bridging and snubbing effects [14–16]. Experimental studies [5, 17, 18] further confirm that interlaminar fracture toughness directly impacts SEA, influencing bending resistance, interwall cracks, and progressive crushing behaviour. Recent finite element (FE) models predict delamination crack growth, bridging mechanisms, and the effects of z-pin parameters such as diameter, insertion angle, and density [4, 19, 20]. Cohesive elements and refined mesh strategies have improved predictive capabilities, enabling accurate simulation of damage modes, frictional pull-out, and z-pin debonding. Bianchi [10] introduced cohesive zone elements to model z-pin failure, while LS-DYNA simulations [21] demonstrated that stitching at tube tops leads to catastrophic failure, requiring alternative placement to optimise SEA. Despite advances in numerical modelling, applying z-pinning to CFRP tube axial crushing remains underexplored. More recently, z-pinning techniques have been developed to improve the compression-after-impact performance and delamination crack growth of composite laminates by multi-scale CNF/Z-pin reinforcement [22] and a Pre-Hole Z-pinning (PHZ) process [23].

Building on previous research [24] the primary goal of this study is to enhance the energy absorption capabilities of composite crash absorbers without increasing weight, specifically investigating how z-pinning influences interlaminar fracture toughness and crashworthiness performance. The research objectives include analysing composite crash absorber deformation under axial loading, developing a z-pinning arrangement to improve crash performance, formulating numerical models to predict deformation patterns, and examining the influence of parameters such as contact definition, material properties, shell count, mesh density, failure initiation, and delamination representation. Additionally, this study evaluates the impact of z-pinning density, diameter, and location in CFRP tubes for crashworthiness

improvements. The findings will contribute to advancing through-thickness reinforcement techniques, refining finite element modelling approaches, and establishing design guidelines for high-performance impact-resistant composite structures.

2 Experimental Studies

2.1 Manufacturing of Crash Tube Specimens

Three CFRP crash tubes were manufactured using a prepreg layup process, where unidirectional carbon-epoxy prepreg plies were wrapped around a mandrel, vacuum-bagged, and cured in an autoclave to ensure high-quality consolidation. The tubes were fabricated without z-pins, allowing for an unreinforced baseline to be used as validation data. The tube specimens dimensions are shown in Fig. 1. The tubes have an outer diameter of 95.72 mm, an inner diameter of 90 mm (conditioned by the mandrel at our disposal at the time of the study), and a nominal wall thickness of 2.86 mm, with a total height of 80 mm. The height of the tubes (80 mm) ensures a sufficient crushing length, allowing for progressive failure modes (splaying, lamina bending, and fragmentation) to develop while remaining within the constraints of our drop-weight impact test setup in Section 2.2. Each tube is made of ten equally thick plies (0.286 mm per ply) arranged in a quasi-symmetric stacking sequence: $[-45/+45/0/+90/0]$ s displayed in Fig. 2. This layup configuration was specifically chosen to achieve balanced mechanical properties and a controlled progressive crushing response under axial impact as well as allowing the use of experimental results for the z-pins bridging properties measured on a similar 3 mm thick carbon-epoxy laminate as explained in Section 4.1. The $\pm 45^\circ$ plies facilitate splaying, the 0° plies provide axial load-bearing capacity, and the 90° plies enhance transverse stiffness, reducing the risk of premature failure. The quasi-symmetric design further minimizes bending and twisting, ensuring a stable, predictable crushing mode, which is a key requirement for crashworthiness applications. A 45° bevel trigger mechanism was integrated at the upper end of the tubes to initiate localized failure, ensuring a stable progressive crushing response rather than catastrophic brittle fracture. This feature plays a critical role in energy absorption efficiency [25, 26], as it directs the failure mode towards a sustained crushing plateau, enhancing the structure's specific energy absorption (SEA). The bevel geometry was selected based on previous crashworthiness studies, where similar configurations have improved consistency in energy dissipation during impact. The material system used in this study is MTC510-UD300-HS-33%

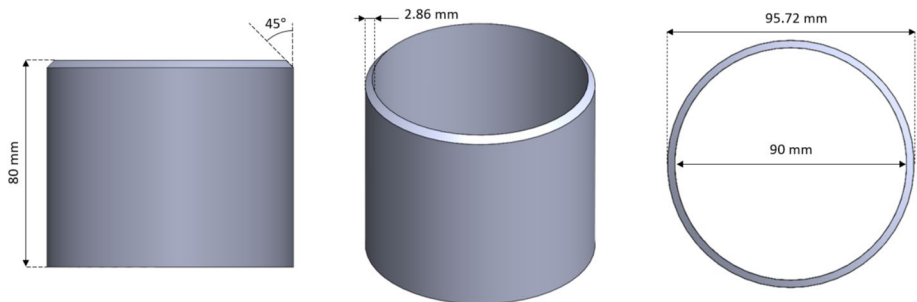


Fig. 1 CFRP composite tube geometry

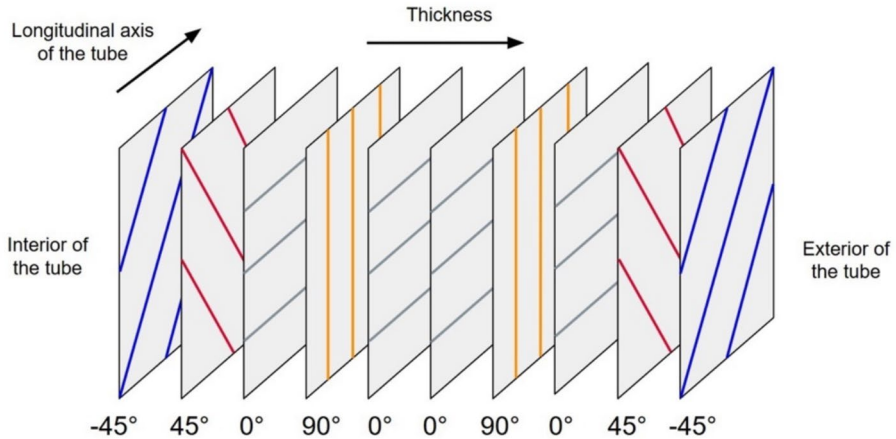


Fig. 2 3D cross-sectional schematic of the CFRP composite stacking sequence and fiber orientation

Table 1 Material properties for each ply (given by the material supplier)

E_1 (GPa)	E_2 (GPa)	$G_{12} = G_{31}$ (GPa)	G_{23} (GPa)	ρ (Kg/m ³)	V_f (%)
116.5	8.8	3.6	3	1540	60
XC (MPa)	XT (MPa)	YC (MPa)	YT (MPa)	USS (MPa)	ν_{12}
-1100	2282	-200	54	99	0.34

E1: Longitudinal Young's Modulus, E2: Transverse Young's Modulus, $G_{12} = G_{31}$: In-plane Shear Modulus, G_{23} : Out-of-plane Shear Modulus, ν_{12} : Major Poisson's Ratio, ρ : Density, V_f : Fiber Volume Fraction, XC: Longitudinal Compressive Strength, XT: Longitudinal Tensile Strength, YC: Transverse Compressive Strength, YT: Transverse Tensile Strength, USS: Ultimate Shear Strength

RW, a high-strength carbon-epoxy prepreg supplied by SHD Composites. The mechanical properties of individual plies are provided in Table 1. This selection was based on CFRP's high specific strength and stiffness, making it well-suited for lightweight impact-resistant structures.

2.2 Impact Testing

The impact test employed a 108.34 kg mass dropped from a height of 2.0 m in a drop tower (Fig. 3-B), ensuring an initial impact velocity of 7.04 m/s and a kinetic energy of 2685 J across all tested specimens. Dynamic load cell data, recorded at 20000 Hz with an accelerometer, captured the load time curve, while the accelerometer sensor was attached on the rear side of the impactor. Progressive crushing, characterized by controlled specimen crushing with sustained compression load-bearing ability, revealed an initial peak load followed by a plateau. This process induced localised failure, interlaminar cracks, and various failure modes, including transverse shearing and lamina bending. Progressive crushing modes were categorized as fragmentation (laminate wedge shape, short interlaminar cracks), lamina bending (long parallel cracks, continuous frond formation), and brittle fracture (a combination of the first two), with the last mode exhibiting the highest energy absorption, particularly in composite tubes (Table 2).

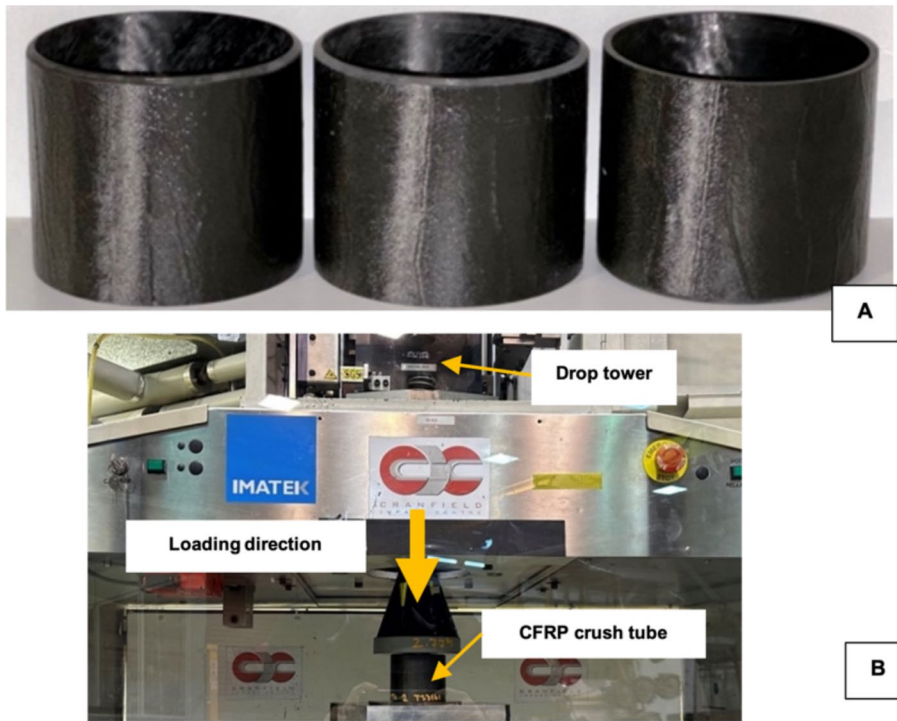


Fig. 3 **A** Final test samples. **B** Experimental impact setup @Cranfield Impact Centre

Table 2 Experimental crashworthiness parameters (average of the three tested specimens)

$F_{max}(kN)$	$F_m(kN)$	$d(mm)$	$SEA(J/g)$	$CFE(\%)$
136 ± 6	78 ± 3	34 ± 1	26.32	73 ± 2

2.3 Experimental Test Results

Figure 4-a displays the experimental force vs displacement diagram for unpinned specimens. Figure 4-b shows the kinetic energy over time evolution for the experimental tests. Three main areas appear: an initial linear segment (O to A) dominated by the trigger-induced failure mechanism, progressive crushing (A to B) with stable crushing and substantial mean load, and a load increase at C due to debris accumulation in the central crack, forming the 'compaction zone'. Crashworthiness results (Table 3) include peak force (F_{max}), mean force (F_m), maximum displacement (d), SEA, and CFE. The predominant failure mechanism under impact is attributed to brittle fracture of the plies.

SEA measures the energy absorbed per unit mass of the crash tube and is given by:

$$SEA = \frac{E_{tot}}{m} \quad (1)$$

where:

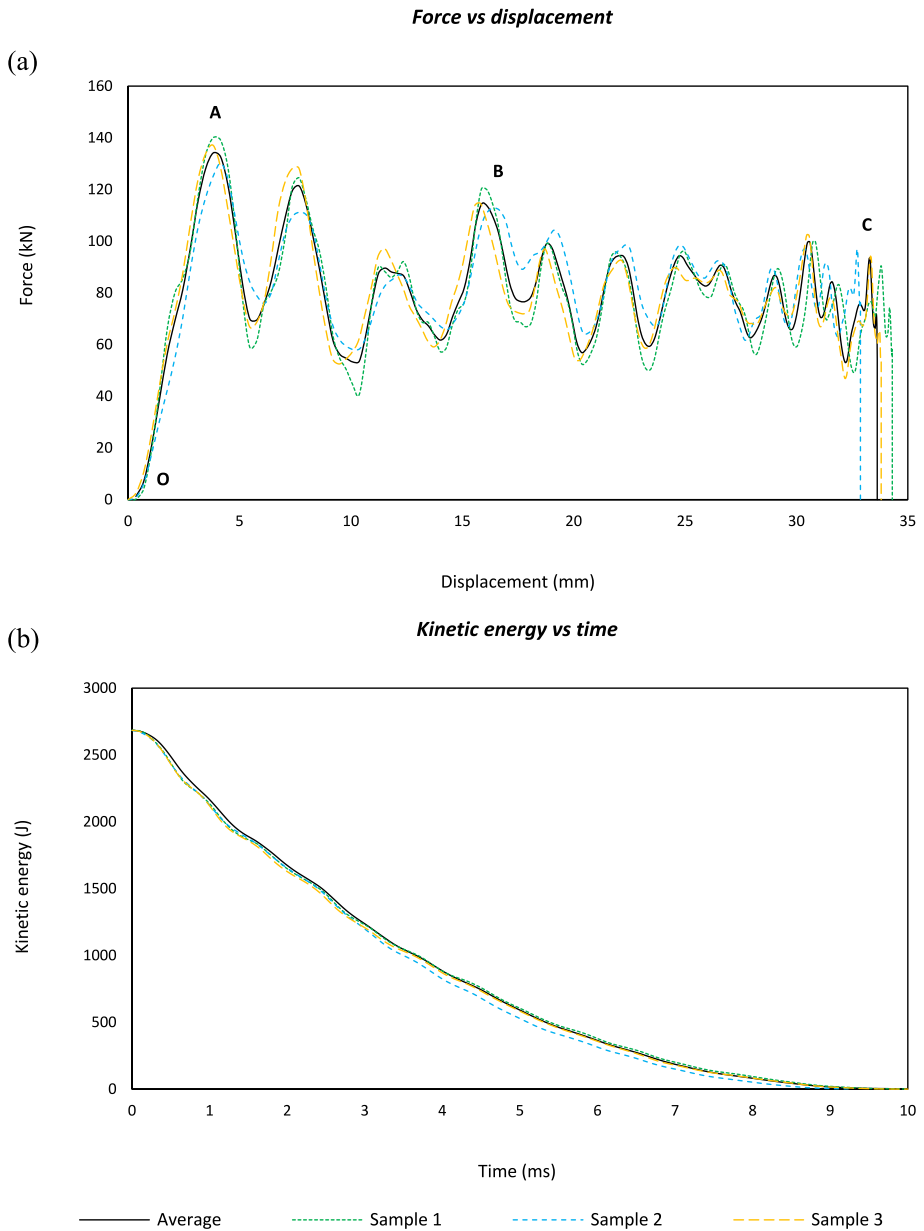


Fig. 4 Test results of experimental studies for three samples

Table 3 MAT54 numerical damage parameters

<i>DFAILM</i>	<i>DFAILS</i>	<i>DFAILT</i>	<i>DFAILC</i>	<i>SOFT</i>	<i>TFAIL(ms)</i>
0.75	0.15	0.18	-0.2	0.9	2E-07

- E_{tot} = Total absorbed energy (Joules)
- m = Mass of the crash tube (Kg)

The absorbed energy E_{tot} is typically calculated as:

$$E_{tot} = \int F ds \quad (2)$$

where:

- F = Axial crushing force (N)
- ds = Incremental crushing displacement (m)

CFE evaluates the stability of the crushing process and is defined as:

$$CFE = \frac{F_{avg}}{F_{max}} \quad (3)$$

where:

- F_{avg} = Average crushing force (N)
- F_{max} = Peak crushing force (N)

The average crushing force is computed as:

$$F_{avg} = \frac{E_{tot}}{s} \quad (4)$$

where s is the total crushing displacement.

Axial and transverse fragmentation at internal and external fronds enhances energy absorption. As observed in Fig. 5, the crushed tube features distinct internal and external mid-walls, separated by a "debris wedge" formed by Mode-I crack propagation. Debris accumulating at the crack tip during impact defines the "debris wedge".

3 Numerical Modeling

3.1 Method of Approach

All models simulate dynamic axial crushing of z-pinned and unpinned CFRP composite crash absorbers using LS-Dyna. To enhance computational efficiency, the composite tube is modelled with two cylindrical shells, as shown in Fig. 6, avoiding penetration and a gap equal to half the total wall thickness. Tube thickness is 2.86 mm with ten 0.286 mm-thick plies. The impactor was modelled as a $120 \times 120 \times 10$ mm rigid solid plate with a mass of 108.34 kg. The material was defined using MAT_020 (Rigid) in LS-DYNA, assigning an arbitrarily large Young's modulus and a 752361 kg/m³ density to achieve the desired mass.

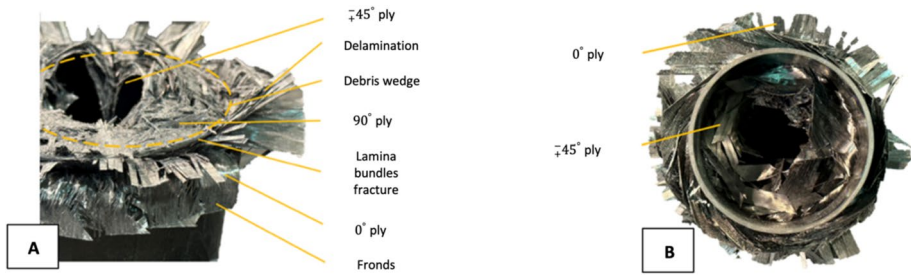


Fig. 5 Tested unpinned specimen according to the experimental setup from Fig. 3-B, **A** iso view, **B** bottom view

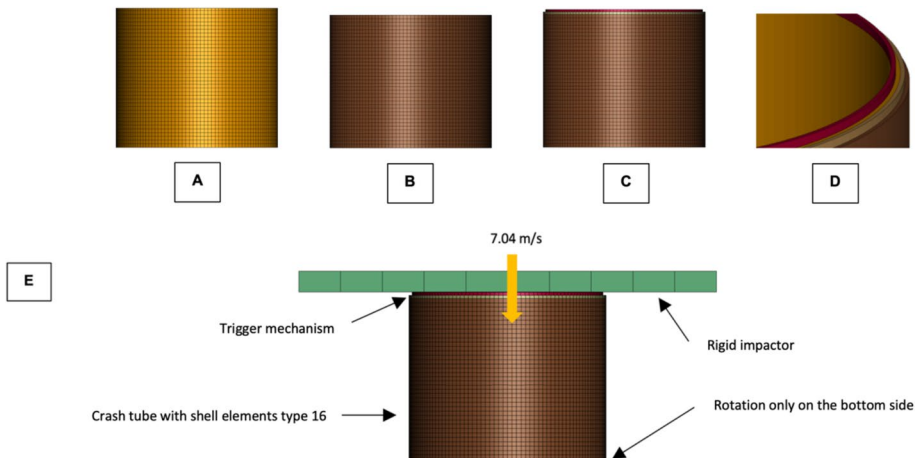


Fig. 6 FE Model geometry: **A** inner shell with reduced inner thickness row, **B** outer shell with reduced outer thickness row, **C** total assembly, **D** bevel trigger with shell thicknesses, **E** FE model, profile view

An initial velocity of 7.04 m/s was applied along the y-axis. The rigid impactor and chosen dimensions ensured full and consistent contact with the tube throughout the crushing process, preventing potential contact instabilities or penetration issues. Following a mesh sensitivity analysis, quadrilateral mesh elements of 2.1×2.1 mm were chosen for the composite tube to ensure an appropriate representation of failure mechanisms. Smaller element sizes did not create the correct delamination process, and buckling appeared while larger elements missed the crushing front shape, leading to incorrect results. Since two shells model the 2.86 mm laminate thickness in contact, each shell is 1.43 mm, and the aspect ratio of the elements is $1.43/2.1 = 0.56$. Feraboli et al. [27] used a similar element size of 2.54×2.54 mm with MAT54 and element type 16 for the axial crushing of a composite tape specimen with a 2 mm thick laminate, further supporting the choice of the mesh size in the current study. Fully integrated linear shell elements (type 16) are suitable for membrane and bending effects and capture through-the-thickness variations in stress and displacement with reduced computational resources and without hourglass modes. In the experimental setup, the bottom edge of the specimen was initially free, but due to the applied impact load, friction with the base plate effectively restricted its translation. To reflect this behaviour while

minimising numerical instabilities, the numerical model constrains the bottom edges of the two shells (Fig. 6-E) from translating while allowing rotations. This simplification does not affect the final results. Although an alternative approach could involve explicitly modelling the base plate and defining a contact interaction, this would introduce additional complexity and potential numerical instabilities. The impact load is applied at the upper end by the impactor. Finally, unpinned composite fracture toughness values are $G_{IC} = 0.21 \text{ kJ/m}^2$ and $G_{IIC} = 0.78 \text{ kJ/m}^2$.

3.2 Trigger Mechanism

To numerically model the 45° bevel trigger present on the experimental specimens at the top of the cylinder tubes (Fig. 6-D), two isosceles right triangle shapes were modelled with shells [25]. Each triangle, modelled by a single row of shell elements, has 0.25 times the tube wall thickness (0.715 mm) with an applied inward shell offset. Linked to the main shells through `contact_tied_surface_to_surface` with friction coefficients of 0.1, these rows create a 'stairs' shape as seen in Fig. 7. Previous studies [25, 28] recommend using two or three rows of elements with decreasing thickness for 45° chamfer triggers, mimicking the trigger mechanism to reduce the initial peak load and promote progressive crushing through interlaminar crack initiation and progression.

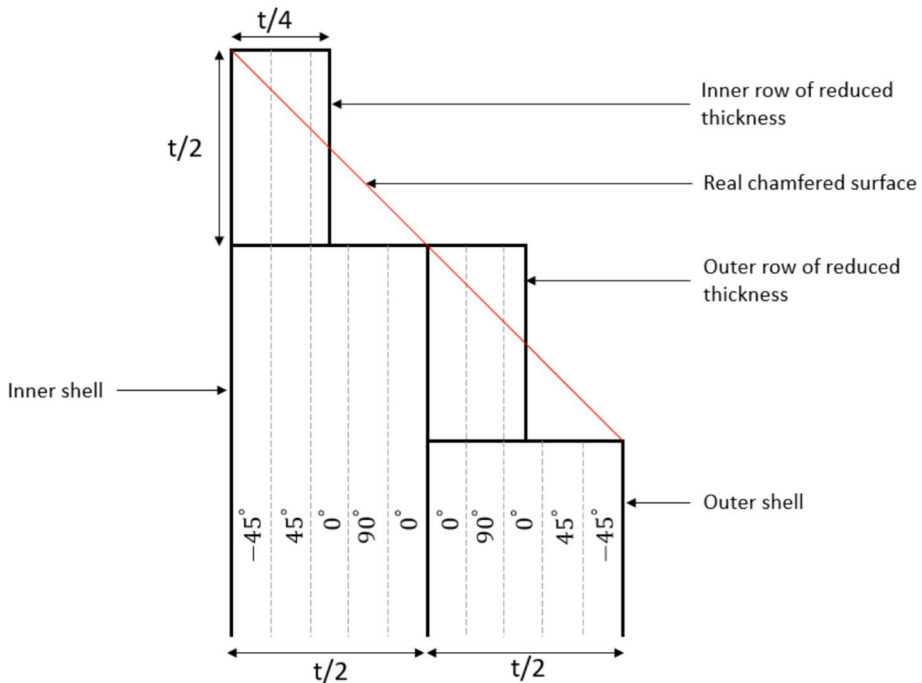


Fig. 7 45° bevel trigger model (wall thickness $t = 2.86 \text{ mm}$)

3.3 Material Model

In simulating composite tube crushing, the MAT54 model with the `mat_enhanced_composite_damage` formulation is well suited [27]. This model employs the Chang-Chang failure criterion to depict matrix and fibre compressive and tensile failure and allows the definition of arbitrary orthotropic materials and unidirectional layers in composite shell structures. The numerical material model parameters in Table 3 were validated against the experimental results in Table 2 and Figs. 4 and 5, using average results from three unpinned CFRP crash tube specimens. The numerical specimens had the same geometry and characteristics as the experimental specimens and were subjected to the loading conditions detailed in Section 3.1. MAT54 damage modelling parameters, DFAILT (fibre tension failure), DFAILC (fibre compression failure), DFAILM (matrix failure), and DFAILS (shear failure) were adjusted starting from values given by the following equations:

$$DFAILT = \frac{X_{t,shell}}{E_{1,shell}}, DFAILC = \frac{X_{c,shell}}{E_{1,shell}}, DFAILM = \frac{Y_{t,shell}}{E_{2,shell}}, DFAILS = \frac{USS_{shell}}{G_{12,shell}}$$

where the $E_{1,shell}$, $E_{2,shell}$, $G_{12,shell}$, $X_{t,shell}$, $X_{c,shell}$, $Y_{t,shell}$, USS_{shell} , are the effective material properties to assign to each shell, i.e. half of the full laminate (5 plies). These material properties are calculated thanks to the material properties of the individual ply given in Table 1. The calibration of the numerical damage parameters starts from the calculated values, then iteratively modifying these values as to enable the numerical results to get closer to the real ones, as well as to get a realistic deformation pattern involving delamination and inward/outward layer spreading (see Fig. 8).

The SOFT parameter has a strong effect on results, weakening elements near damaged or deleted ones, especially at the crash front. Modifying SOFT influences crushing length and enhances energy absorption. The SOFT and TFAIL values were iteratively adjusted taking into account recommendations from [27] and then starting from a value of 0 and gradually increasing the value to align with experimental deformations.

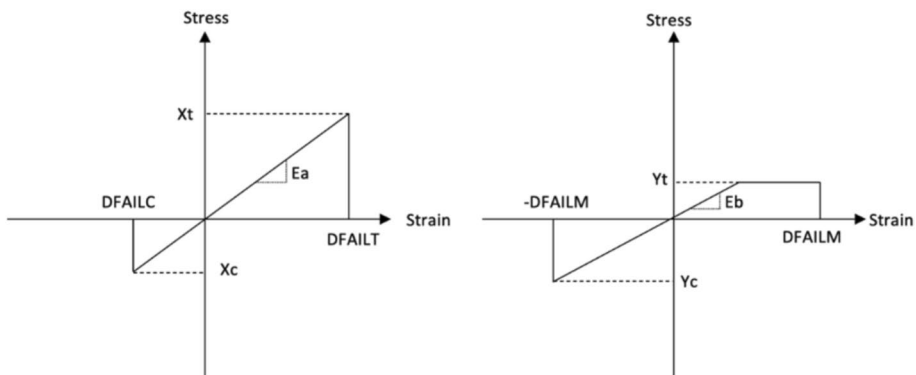


Fig. 8 Stress–strain curves with MAT54, following the explanations in [27] and in the LS DYNA keyword user's manual, volume II, and material models

Table 4 Tiebreak contact parameters for model 1

Option	$G_{IC} (kJ/m^2)$	$NFLS (MPa)$	$SFLS (MPa)$	PARAM (mm)
8	0.21	60	25	0.007

Table 5 MAT54 parameters for model 1

DFAILM	DFAILS	DFAILT	DFAILC	SOFT	TFAIL(ms)
0.75	0.15	0.18	-0.2	0.9	2E-07

3.4 Main Contact Definition for Models 1, 2 and 3

The accurate definition of the contact interface is vital for modelling interactions, preventing unrealistic penetration during deformation. Three types of contacts are employed in this paper. A contact automatic single surface with friction coefficients ($f_s = 0.1$ and $f_d = 0.1$ [29]) prevents shell self-penetration post-impact. Four contact automatic node-to-surface contacts between the impactor and shells, validated with friction coefficients between 0.1 and 0.4 [29]. Optimal friction coefficients between the impactor and specimen (0.3 static, 0.2 dynamic), based on [30], ensure realistic deformation shapes. Contacted surface-to-surface links reduced the thickness of rows to the main shells.

3.5 Model 1, Interlaminar Behaviour Modelled with Energy-Based Contact

The first modelling technique employs the contact automatic_one_way_surface_to_surface_tiebreak (option 8) to simulate Mode-I delamination, crucial for energy absorption [31]. The inner shell is the master, the outer shell the slave. This mechanism transfers compression loads from slave nodes to master segments and tangential loads through friction, with a failure criterion $\left(\frac{|\sigma_n|}{NFLS}\right)^2 + \left(\frac{|\sigma_s|}{SFLS}\right)^2 \geq 1$, where σ_n and σ_s represent normal and shear stress on the contact surface. NFLS and SFLS denote interlaminar strengths for normal and shear loading conditions. Upon reaching a value of 1, contact forces diminish until a gap distance PARAM between the surfaces in contact is reached, indicating interface failure. After failure, it behaves as a contact automatic_surface_to_surface. A sensitivity analysis favours pure Mode-I delamination due to its prominence in energy absorber axial crushing [19]. Static and dynamic friction coefficients are set at 0.3 and 0.2 [32]. This tiebreak contact facilitates interlaminar detachment simulation, preventing layer penetration post-crack propagation. The primary failure mode in axial crushing is Mode I, $G_{IC} = \frac{1}{2}\sigma_n PARAM$, where σ_n is the normal interlaminar strength NFLS (see Tables 4 and 5).

Model 1 is validated with the material numerical parameters from Table 5. The simulated load–displacement and kinetic energy–time curves are displayed in Fig. 9. Table 7 presents the numerical crashworthiness results against experiments. The numerical model could not match the initial peak crushing load (thus CFE), and post-initial peak, the simulated load drop is steeper than in the experimental studies. However, the mean crushing load and SEA align with experimental values, showing less than 10% error (see Table 6).

Figure 10 displays various views of the numerically crushed tube. The double-shell setup cannot perfectly replicate the crushed shape as layers are not modelled separately. While

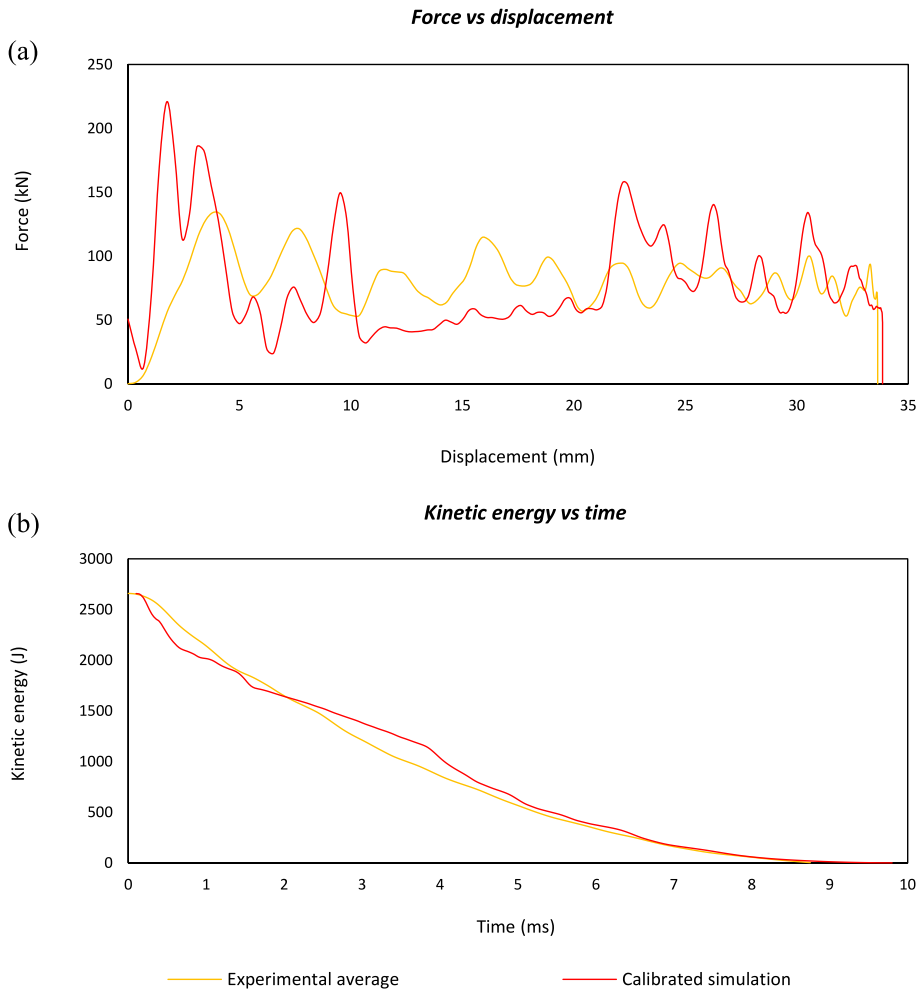


Fig. 9 Comparison of results from experiments and from model 1 (unpinned)

Table 6 Comparison of crashworthiness results, experimental and validated model 1 (unpinned)

	$F_{max} (kN)$	$F_m (kN)$	$d (mm)$	$SEA (J/g)$	$CFE (\%)$
Experimental	121 ± 6	78 ± 3	34 ± 1	26.32	64 ± 2
FEM	220	77	33.8	26.32	35
Difference (%)	81.8	0.65	0.9	0	45.3

failure relies on ply brittleness and Mode-I fracture between shells, petal formation is well-predicted with element deletion. Discrepancies between experimental and FEA stem partly from the petal de-bouncing post-deformation.

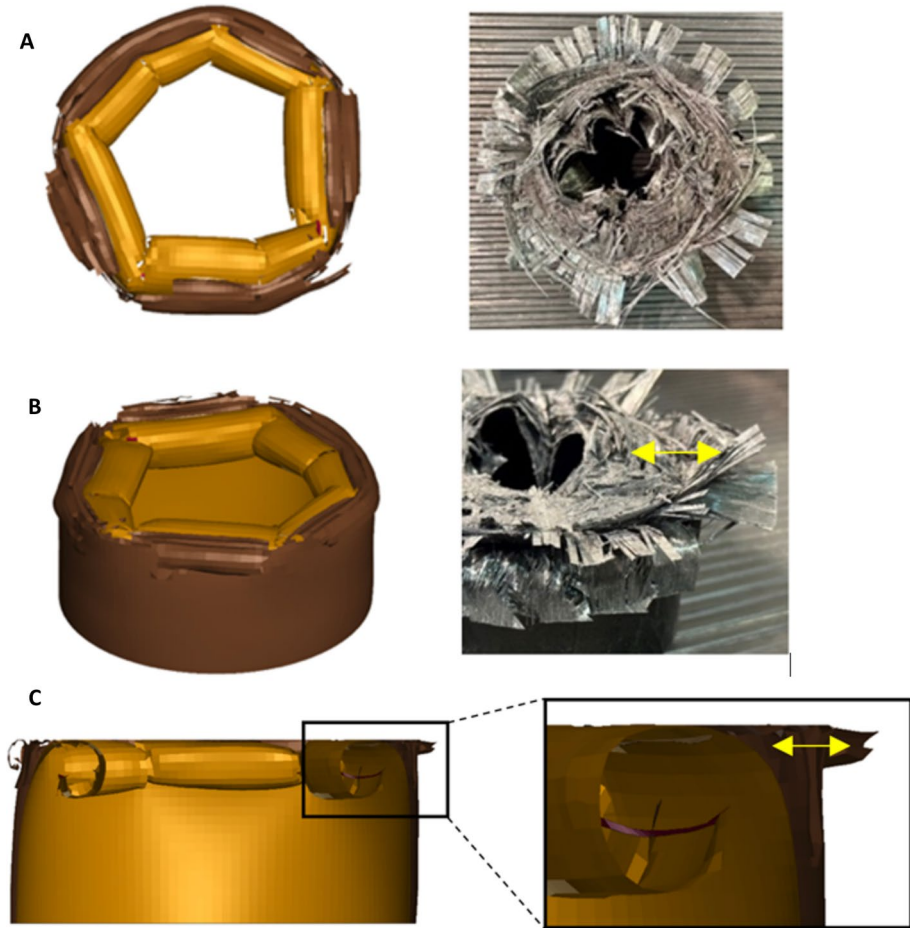


Fig. 10 FEM and experimental crushed morphologies, **A** top, **B** iso, **C** section view

3.6 Model 2, Interlaminar Behaviour Modelled with Cohesive Elements

The second approach incorporates cohesive elements for modelling interlaminar behaviour. A 0.01 mm-thick solid layer with type 19 cohesive elements is added between the two main shells (Fig. 11). These 8-noded, 4-point elements handle complex geometries, seamlessly transitioning from bonded to unbonded states and capturing progressive damage evolution. Extending from the inner shell surface, the solid cohesive layer maintains mesh continuity. The cohesive and shell elements share a 2.1×2.1 mm mesh size. This cohesive element size was revealed to be the most suited after a mesh sensitivity analysis was performed on the cohesive elements. Each normal direction to the cohesive element is crucial for accurately representing normal and tangential behaviours. The MAT184 cohesive elastic model employs traction separation laws to depict material response, specifying maximum strengths, stiffness (G_{IC} and G_{IIC}), and conditions for cohesive element failure

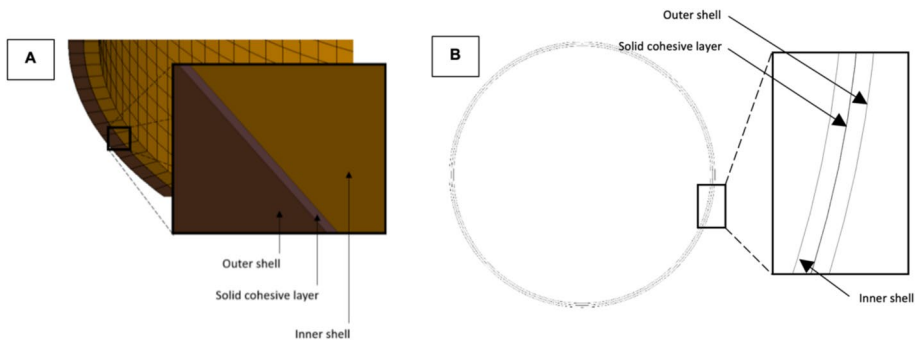


Fig. 11 **A** Cohesive elements between the main shells. **B** In the bottom view, shell thicknesses are not shown

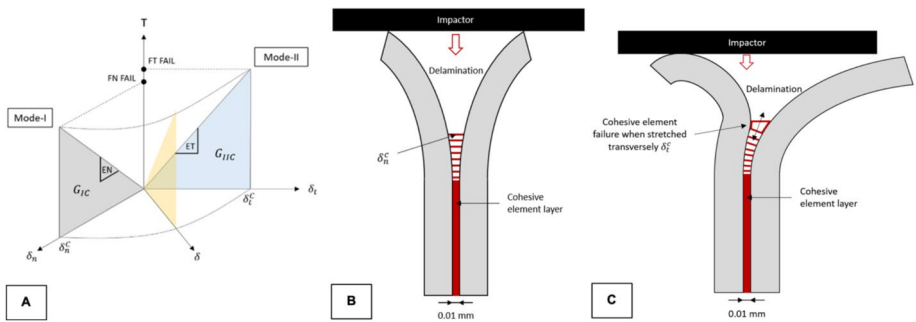


Fig. 12 **A** MAT184 load displacement law. **B** Mode-I. **C** Mode-II

Table 7 MAT184 parameters for validated model 2

G_{IC} (kJ/m ²)	EN (kN/mm ³)	ET (kN/mm ³)	$FN\ FAIL$ (MPa)	$FT\ FAIL$ (MPa)	$INT\ FAIL$
0.21	8.57143	0.4	60	25	1

and delamination initiation as observed in Fig. 12. Table 7 presents the cohesive material properties for the numerical model.

The mixed-mode cohesive law in Fig. 12-A captures different behaviours for normal and tangential displacement components in Mode-I and Mode-II, respectively. Parameters like $FN\ FAIL$, $FT\ FAIL$, δ_n^c and δ_t^c denote normal and tangential ultimate strength of cohesive elements, as well as normal and tangential displacement separation. Energy release rates, G_{IC} and G_{IIC} , are expressed as $G_{IC} = \frac{1}{2} FN\ FAIL * \delta_n^c$ and $G_{IIC} = \frac{1}{2} FT\ FAIL * \delta_t^c$, where δ_n^c and δ_t^c are the maximum displacements at interface failure. The model considers EN and ET as normal and tangential cohesive stiffnesses. The cohesive layer attaches to interior and exterior shells through `contact_tied_shell_edge_to_surface_constrained_offset`, with inner and outer composite shells as masters. This contact has static and dynamic coefficients of friction set to 0, activated when the tied contact fails and behaves as a surface-to-surface contact. Cohesive elements may experience high distortions, especially at final crushing states (see Fig. 13), impacting calculation times. However, these highly distorted elements

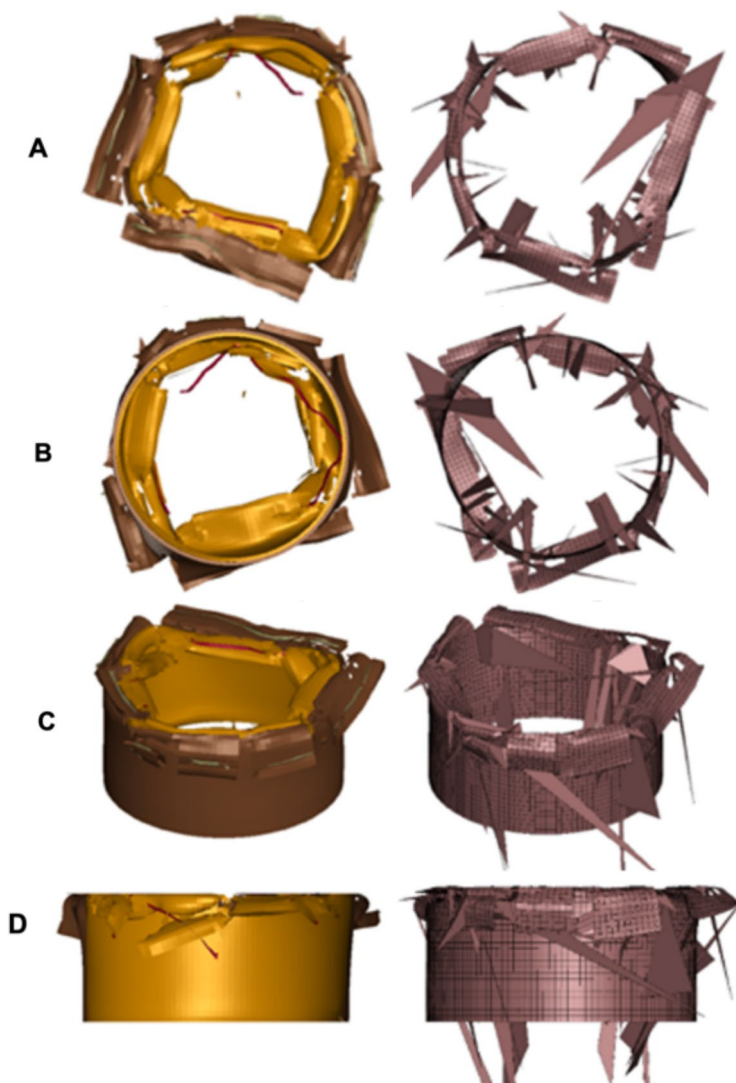


Fig. 13 Model 2 (Unpinned), final deformation for main shells and cohesive elements, **A** top, **B** bottom, **C** iso, **D** section view

Table 8 MAT54 parameters for model 2

<i>DFAILM</i>	<i>DFAILS</i>	<i>DFAILT</i>	<i>DFAILC</i>	<i>SOFT</i>	<i>TFAIL(ms)</i>
0.75	0.15	0.017	-0.25	0.9	2E-07

fail and are thus not considered in the crushing process. These distortions are mitigated by adjusting the timestep. Table 8 presents the material parameters for the numerical model, and Table 9 and Figs. 13 and 14 present the results. Model 1 could not replicate the initial peak crushing force, while the mean crushing force and the SEA are correctly matched.

4 Z-pinning Implementation

4.1 Z-pin Bridging Behaviour

Z-pinned laminate fracture toughness depends on factors like pin configuration, material, diameter, length, insertion angle, and inherent laminate fracture toughness [19]. This study focuses on T300/BMI z-pins, a popular type of z-pins [19, 20, 33] with diameters of 0.28 mm or 0.51 mm. Z-pins are supposed to be symmetrically embedded in the stacking direction of the laminate (Fig. 14), allowing detachment from either half of the laminate thickness, and are modelled at the main interlaminar fracture plane in tubes under impact. Analytical bridging models, based on z-pin pull-out and DCB tests, help determine z-pinned interlaminar fracture toughness [5, 6, 34]. Common pin areal densities are 0.5% or 2%, the percentage of pin-covered surface. Three modelling approaches were explored for pinned areas: uniform mesh with energy-based contact thanks to model 1, uniform mesh with cohesive elements thanks to model 2, both averaging the interlaminar strength of the laminate and the z-pin bridging strength across the pinned areas; Individual modelling of z-pins with local energy-based contacts and a refined mesh at pins locations to accommodate the discrete z-pin bridging was developed in model 3. Z-pinning properties in Fig. 16 and Table 11 originates from experimental DCB tests on T300/BMI pins in a 3 mm thick carbon-epoxy prepreg laminate [9]. These experimental values were used for this study as access to z-pins was not possible at the time of this work. The 2.86 mm thick carbon-epoxy prepreg laminate of the current study has only 5% difference in thickness compared to the material used in [9] and it is considered reliable to use these z-pinning properties for the numerical models (see Table 10). The Mode-I fracture toughness of the z-pinned material was established thanks to [9] and used for models 1 and 2 as an averaged delamination property across the z-pinned areas (see Figs. 15 and 16). The individual pin pull-out strength sourced from [35] was used for individual pin modelling in model 3. The pull-out loads $P_{max, pin}$ are 35 N and 33.5 N for 0.28 mm and 0.51 mm pins respectively, with a maximum displacement before pull-out $Disp_{max}^{pin}$ of 1.5 mm for both [5]. The energy release rate for the z-pin interface is calculated as $G_{IC}^{pin} = \frac{1}{2} P_{max, pin} Disp_{max}$.

4.2 Z-pinning Modelling with Model 1

Model 1 used an energy-based contact for the entire structure, pinned areas had increased interlaminar strength compared to the unpinned areas as in Table 11. The energy-based contact tiebreak defines Mode-I fracture toughness as $G_{IC}^{pinned} = \frac{1}{2} \sigma_{n, max} PARAM_{pinned}$ where $PARAM_{pinned}$ is the maximum allowed distance between areas in contact before transforming into simple surface-to-surface contact without interlaminar strength. Calculating $PARAM_{pinned}$ for different z-pinning configurations is feasible. The normal bridging strength $\sigma_{n, max}$ (NFLS) in pinned regions is given by $\sigma_{n, max} = (1 - x_p) \sigma_{n, CFRP} + x_p \sigma_{n, pin}$ where $\sigma_{n, CFRP}$ is the interlaminar bridging

Table 9 Comparison of crashworthiness results, experimental and model 2 (unpinned)

Parameters	F_{max} (kN)	F_m (kN)	d (mm)	SEA (J/g)	CFE (%)
Experimental	121 ± 6	78 ± 3	34 ± 1	26.32	64 ± 2
FEM	215	78	33.7	26.32	36
Difference (%)	77.7	0.5	0.6	0	43.7

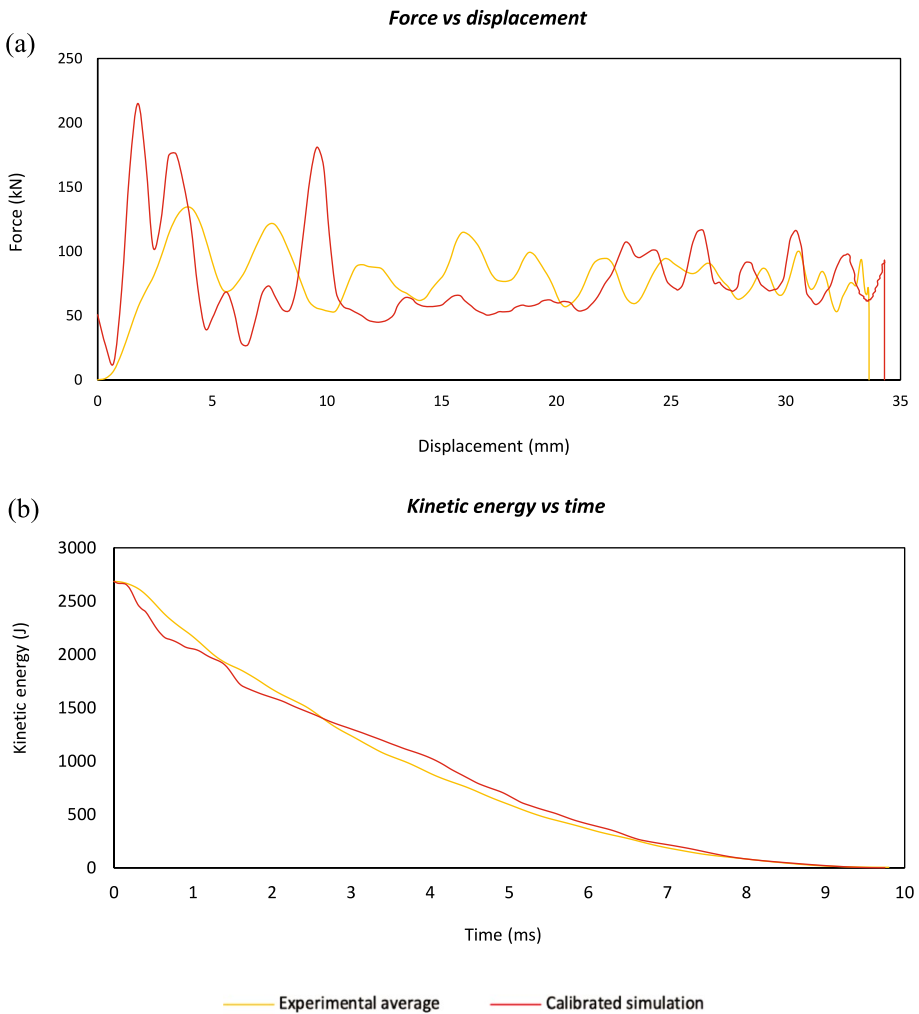


Fig. 14 Comparison of results from experiments and from model 2 (unpinned)

strength of the unpinned composite material, $\sigma_{n,pin}$ is the maximum bridging strength of individual pins, and x_p is the areal density of z-pins (in %). Thus, the contact strength is an average of the unpinned composite material's interlaminar strength and each pin's bridging strength. For the unpinned composite, $G_{IC} = 0.21 \text{ kJ/m}^2$, $G_{IIC} = 0.78 \text{ kJ/m}^2$, the normal strength $\sigma_{n,CFRP}(\text{NFLS})$ is 60 MPa, and the shear strength $\sigma_{s,CFRP}(\text{SFLS})$ is 25 MPa. Due to limited experimental data, the assumption is that the presence of each pin does not increase G_{IIC} ; thus, Mode-II energy release rate remains 0.78 kJ/m^2 for the entire model. Limitations include the inability to define different maximum displacements for normal and tangential behaviours, and the model addresses the change in PARAM only due to Mode-I failure (Table 11 and Fig. 17).

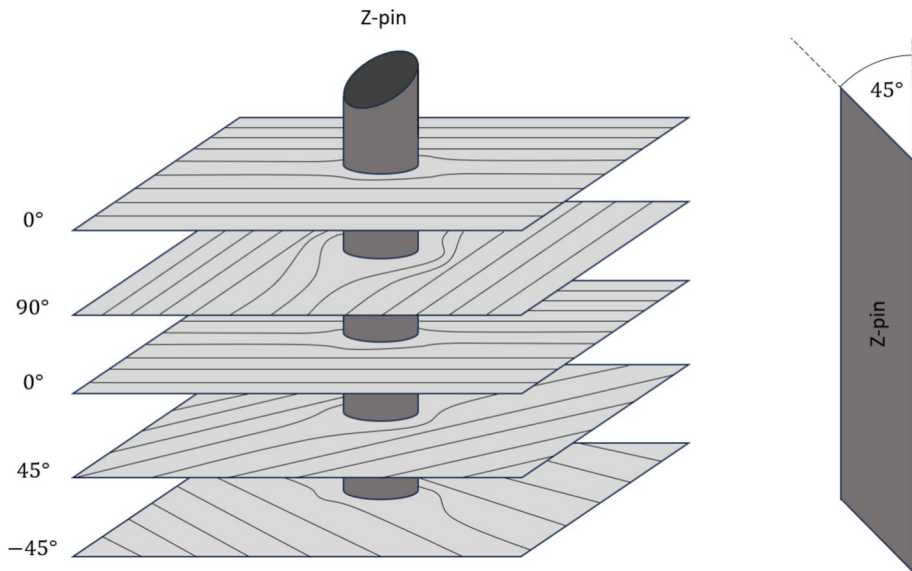


Fig. 15 Schematic of an inserted z-pin into an orthotropic laminate

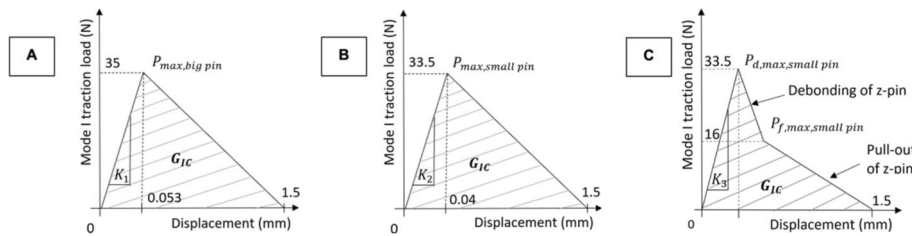


Fig. 16 Idealized bilinear and trilinear load–displacement laws for z-pin bridging in a 3 mm thick laminate with a 3 mm inserted pin length, **A** pin diameter of 0.51 mm, **B** pin diameter of 0.28 mm, **C** pin diameter of 0.28 mm

4.3 Z-pinning Modelling with Model 2

To establish the properties of cohesive elements in pinned areas the previous averaging method is employed. The normal bridging strength FN_FAIL in pinned regions can be calculated as $\sigma_{n,max} = (1 - x_p) \sigma_{CFRP} + x_p \sigma_{pin}$. Then, the normal stiffness EN of the cohesive elements is calculated such as G_{IC} has the desired value for the unpinned or pinned

Table 10 Mode-I fracture toughness G_{IC} for different z-pinning configurations (right). Maximum bridging stresses of single z-pins (left)

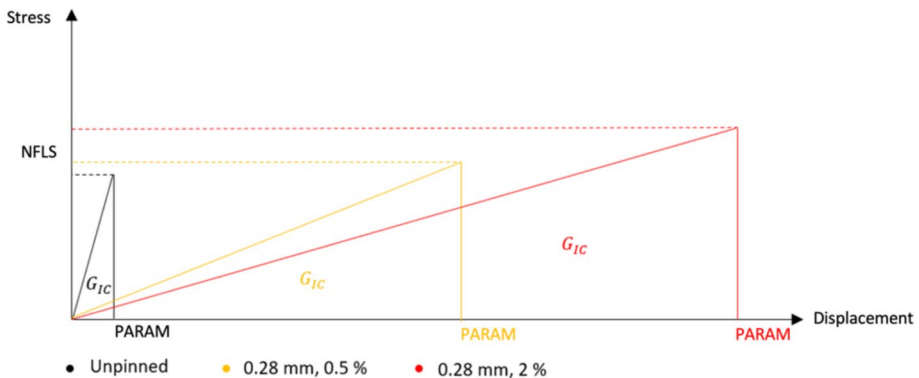
Z-pin configuration	$G_{IC} (kJ/m^2)$	Pin diameter	Pull-out strength (MPa)
(I) 0.28 mm/0.5%	1.2 [5]		
(II) 0.28 mm/2%	5 [5]	0.51 mm	230 [34]
Unpinned	0.21	0.28 mm	420 [34]

Table 11 Energy-based tiebreak contact is used in unpinned and pinned areas (model 1)

Z-pinning configuration	$G_{IC}(kJ/m^2)$	$PARAM(mm)$	$NFLS(MPa)$	$SFLS(MPa)$
(I)	1.2	0.038835	61.8	25
(II)	5	0.1488	67.2	25
Unpinned	0.21	0.007	60	25

area. The possibility of modelling Mode-I and Mode-II independently allows the use of different stiffnesses for normal and tangential behaviours rendering this model more suitable for realistic simulations. Two material models are assigned, to the pinned area and the unpinned area separately (Table 12 and Fig. 18).

Model 2 differs from Model 1 by necessitating multiple solid parts with distinct properties for cohesive zones representing various mechanical attributes for pinned and unpinned surfaces (Fig. 24). This method increases computation time fivefold due to added solid elements and deformations in cohesive elements during tube crushing, leading to inconsistent timestep values in explicit analysis. To depict diverse z-pinning designs, multiple cohesive layer sections are used with properties detailed in Table 12. To address instabilities, an automatic surface-to-surface contact is established at the interface between pinned and unpinned solid cohesive parts, without the need for friction coefficients. This approach enhances computational efficiency and result accuracy for a more consistent crushing process. Each z-pinned design using distinct cohesive parts requires validation. Due to time constraints, not every design (with only unpinned properties) could be validated against

**Fig. 17** Traction-displacement laws in pure Mode-I for the energy-based tiebreak contact (not to scale)**Table 12** MAT184 cohesive elastic properties used in unpinned and pinned areas (model 2)

Z-pin diameter/Z-pin Areal density	$G_{IC}(kJ/m^2)$	$EN(kN/mm^3)$	$ET(kN/mm^3)$	$FN_{FAIL}(MPa)$	$FT_{FAIL}(MPa)$
(I) 0.28 mm/0.5%	1.2 [5]	1.59135	0.4	61.8	25
(II) 0.28 mm/2%	5 [5]	0.415296	0.4	67.2	25
Unpinned	0.21	8.57143	0.4	60	25

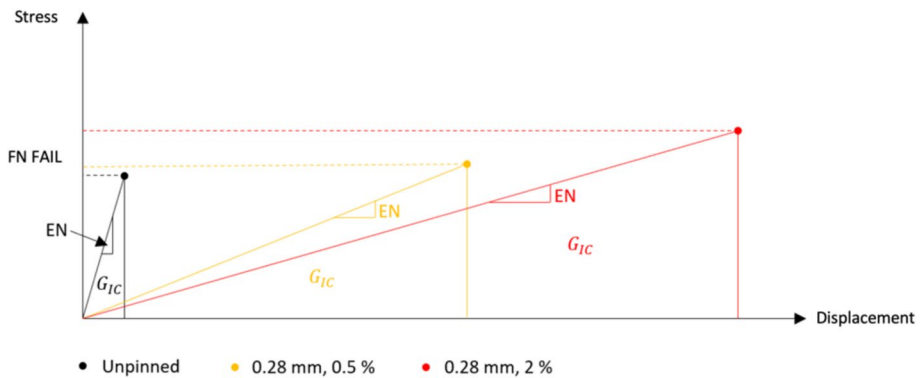


Fig. 18 Traction-displacement laws in pure Mode-I for MAT184 cohesive material

experimental data. After validating the unpinned models, pinned properties are assigned to the appropriate cohesive section.

4.4 Z-pinning Modelling with Model 3

Model 3 aims to model the z-pins individually; the pinned region employs a locally refined mesh at each pin location (Fig. 19), using two type 16 thick main shells, like Models 1 and 2, with consistent bevel triggers and material model parameters. Z-pins are represented as circular areas on these shells as seen in Fig. 19-C and in Fig. 20, mirroring their real cross-section and placed at specific locations (Fig. 19-A and B) defined by the z-pinning density and pin diameter. SolidWorks CAD software simplifies the design process, creating circular pin holes in the main cylinder using circular and linear patterns. The circular areas representing individual pins on the inner and outer surfaces of the tube are perfectly aligned, ensuring the locally refined mesh defining z-pin contact aligns as well. The assembly is exported to LS Prepost as an IGS file, where the interior and exterior meshes for the shells are generated with the deviation option in LS Prepost. The mesh in the unpinned area is 2.1×2.1 mm. At each pinned location, four shell elements intersect to enhance mesh stability. The Solidworks-based method to create the geometry mitigates penetration and node instability issues when the mesh is applied. For this study, Model 3 analysed 0.51 mm diameter pins with 0.5% areal density, while Models 1 and 2 used 0.28 mm diameter pins. Models 1 and 2 relied on DCB test data [9], while Model 3 utilized pin pull-out test data from the literature [35]. The distinction in modelling techniques (Models 1, 2, 3) accommodates either DCB tests or pull-out tests. Model 3 was not suited for 0.51 mm diameter pins with 2% areal density or 0.28 mm diameter pins with either 0.5% or 2% areal density as the mesh needed to be refined to accommodate smaller pins or higher density. Numerical instabilities appeared, the calibration of the model was not possible and the delamination process could not be represented, so only 0.51 mm/0.5% was studied. Model 3 employs an energy-based tiebreak contact (option 8) for the entire structure, enhancing Mode-I delamination resistance as the crushing front reaches local pinned areas. Contact parameters for unpinned and pinned locations are specified in Table 13.

Table 13 values are from pull-out tests, providing Mode-I energy release rates for the tiebreak contact in this model. Unlike Models 1 and 2, specific local bridging is employed

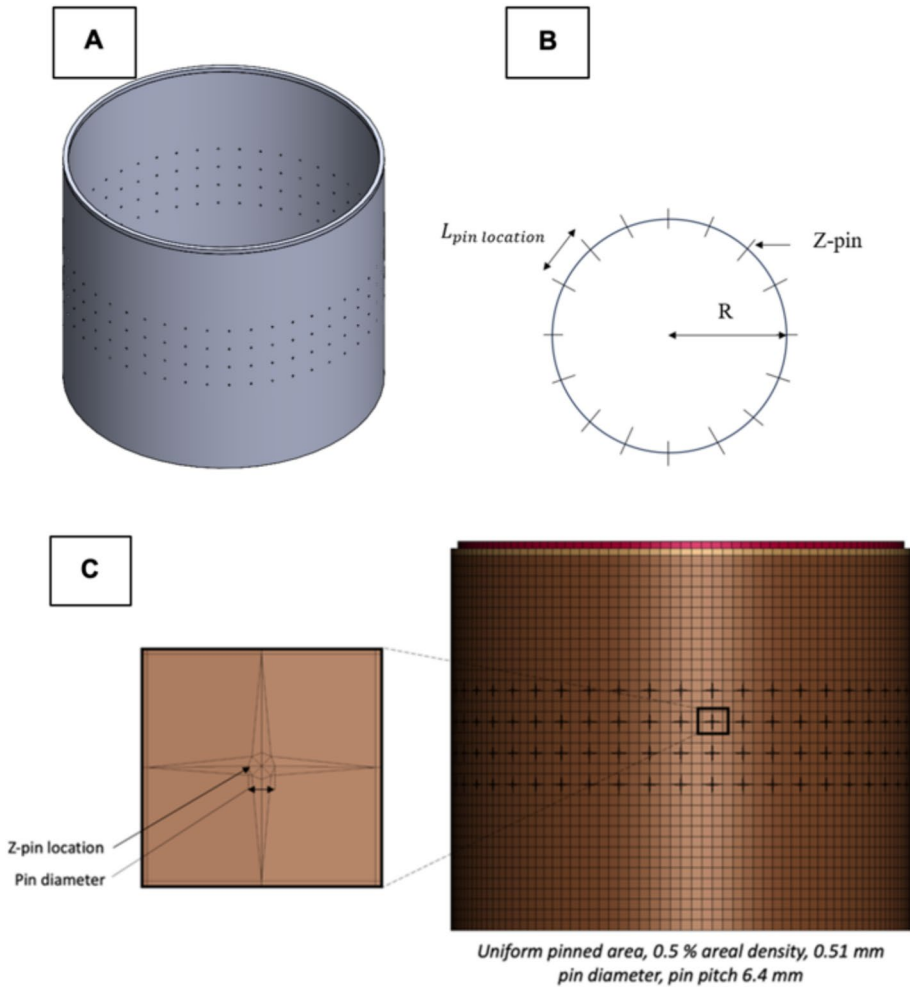


Fig. 19 **A** SolidWorks assembly for the whole tube. **B** Z-pin locations. **C** Mesh system for Model 3

to avoid the averaging method over pinned areas. Figure 20-A shows contact regions for unpinned and pinned bridging (Fig. 21).

Model 3 was validated against unpinned experimental tests with material parameters in Table 14. The same mesh is used for both unpinned and pinned tubes, with contact definitions adapted between pinned and unpinned areas. Compared to models 1 and 2 in their unpinned configurations, unpinned model 3 presents larger deviations in crashworthiness parameters compared to the unpinned experimental results (Table 15 and Fig. 22). These higher differences are attributed to the transition from 2.1 mm to refined 0.1 mm shell elements at pinned locations, causing element distortion. The crushing behaviour of the unpinned tube closely resembles experimental tests, displaying petal formations, axial splitting, and inward bending of the inner shell observed in Fig. 21. Due to time constraints in this study, the further calibration of model 3 against experimental data in its unpinned configuration was not possible.

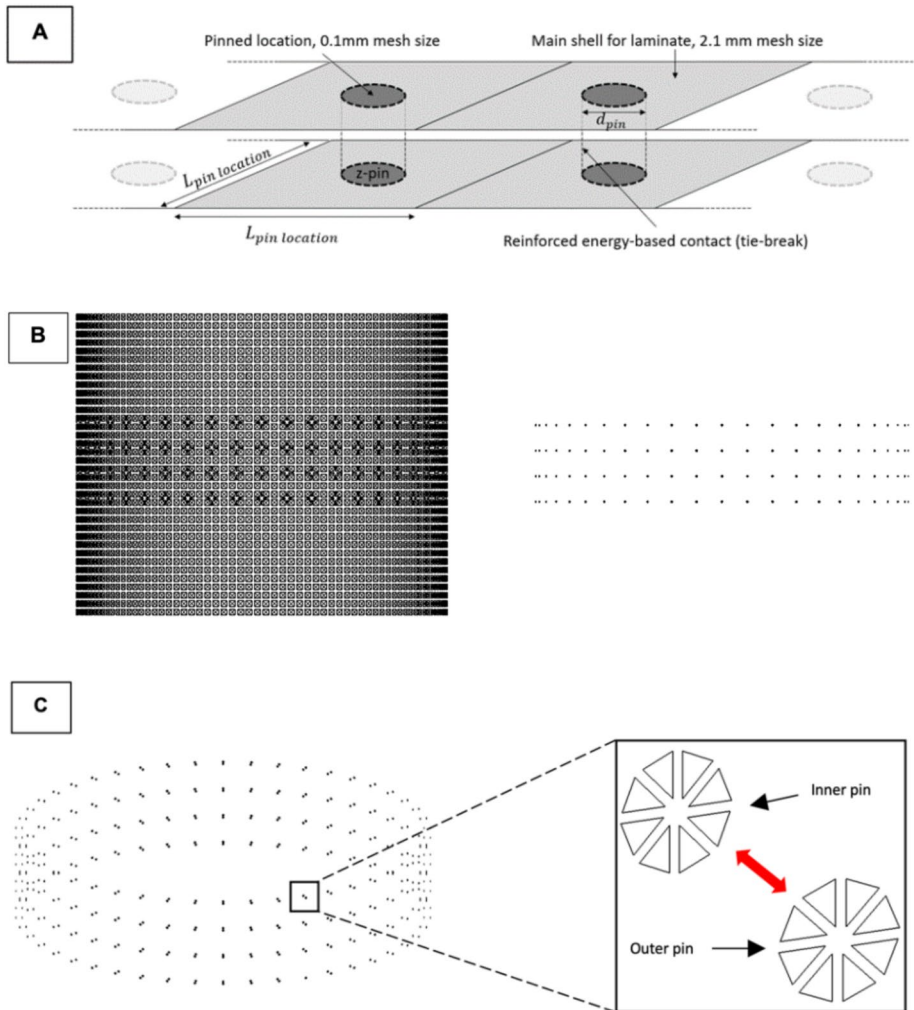


Fig. 20 **A** Schematic of z-pins distribution. **B** Segment-defining elements belonging to the unpinned (left) and pinned (right) areas. **C** Alignment between inner and outer z-pins segments

Table 13 Contact properties for model 3

Z-pinning configuration	G_{IC} (kJ/m ²)	PARAM(mm)	NFLS(MPa)	SFLS(MPa)
0.51 mm/0.5%	164.45	1.43	230	25
Unpinned	0.21	0.007	60	25

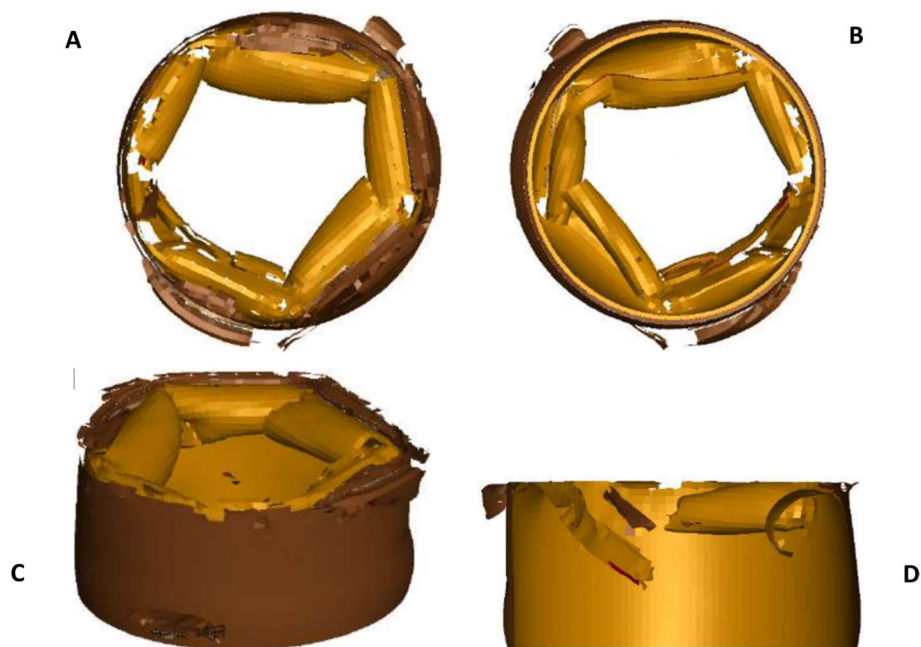


Fig. 21 Unpinned model 3, final deformation shown, **A** top, **B** bottom, **C** iso, **D** section view

Table 14 MAT54 parameters for model 3

$DFAILM$	$DFAILS$	$DFAILT$	$DFAILC$	$SOFT$	$TFAIL(ms)$
0.75	0.15	0.017	-0.25	0.9	2E-07

Table 15 Crashworthiness results comparison, unpinned model 3

Parameter	$F_{max} (kN)$	$F_m (kN)$	$d (mm)$	$SEA(J/g)$	$CFE(\%)$
Experimental	121 ± 6	78 ± 3	34 ± 1	26.32	64 ± 2
FEM	218	64	35	26.32	29
Difference (%)	80.2	17.4	4.5	0	54.7

4.5 Z-pinned Reinforced Designs for Model 1 and Model 2

Five z-pinned CFRP tube designs were suggested and examined using model 1 and 2, represented in Fig. 23. These designs maintain equal total pinned surfaces on the specimen for z-pinning configurations (I) and (II), with uniform pin areal density across pinned areas.

4.6 Z-pinned Reinforced Designs with Different Cohesive Parts

Both Models 1 and 2 share a common mesh system without local refinement at pinned locations. However, the cohesive approach (model 2) requires distinct parts for pinned and unpinned regions for different cohesive properties (Fig. 24). To distinguish the pinned and

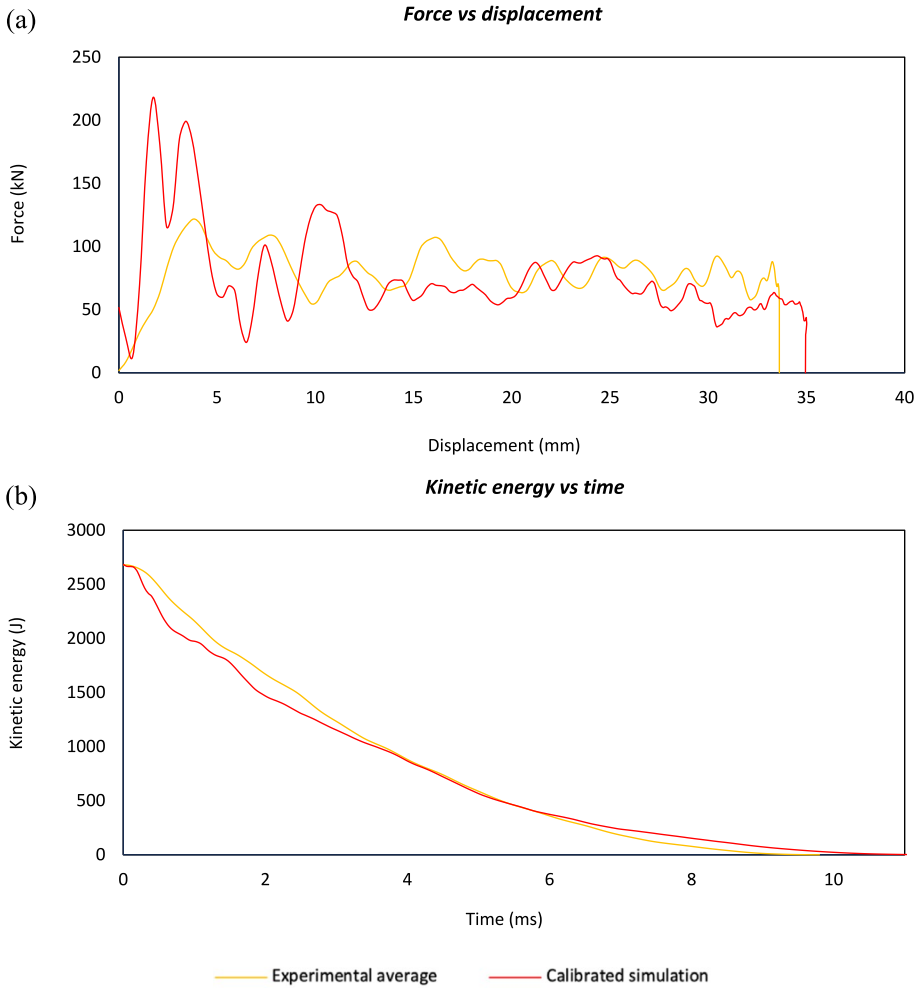


Fig. 22 Comparison of crashworthiness results between experimental and unpinned model 3

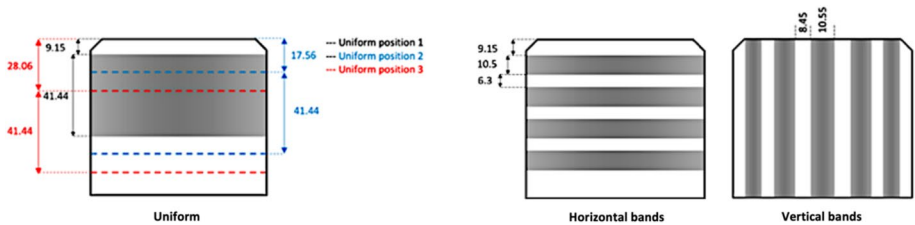


Fig. 23 Pinned areas (gray) with models 1 and 2 (in mm)

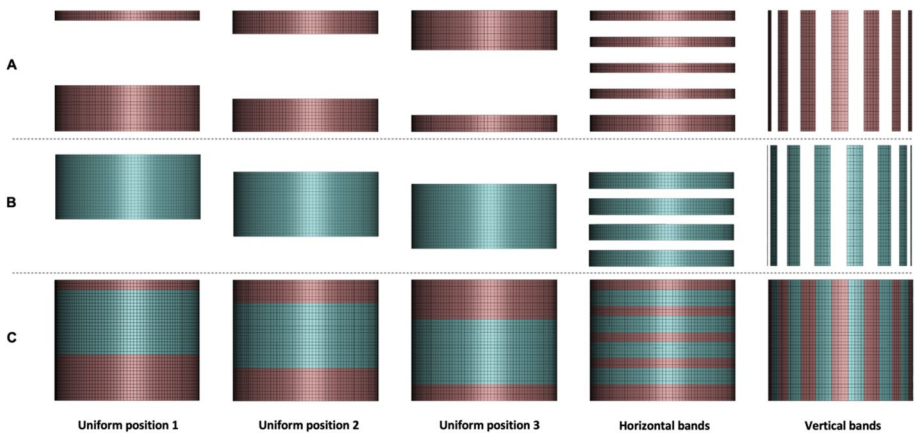


Fig. 24 Cohesive parts for reinforced designs, **A** unpinned area, **B** pinned area, **C** assembly

unpinned cohesive delamination interface, only a cohesive property variation in the normal direction of the laminate was used with solid section type 19. Thus, the different reinforced designs had to be individually checked in their respective unpinned states against unpinned experimental results.

Diverse force–displacement evolutions and crashworthiness parameters emerge when using different parts and assemblies for various designs in the absence of z-pin bridging (see Figs. 25). Individual calibration for each design is needed to ensure closer similarities among unpinned designs, but time constraints for this work have hindered this process. Only a comparison between all unpinned designs was done. While most designs exhibit minimal differences, the 'vertical bands' design displays a notable increase in the final crushing stages and crushing length, requiring further calibration of model 2. Consequently, this design is excluded from comparisons between z-pinned and unpinned tubes. In subsequent analyses, unpinned designs are denoted as 'U', and the two z-pinning properties at our disposal as (I) or (II).

5 Discussion of Numerical Results with Z-pinned Models

Reinforced designs are compared against the unpinned tube. Models 1 and 2 assess z-pinning configurations (I) or (II), using DCB test data for the z-pin bridging averaging method. Model 3 explores larger 0.51 mm diameter pins at 0.5% pin areal density, requiring local definition with a different mesh system due to data being limited to pull-out tests in the literature.

5.1 Crashworthiness Results from Contact Model 1

The following discussed results are presented in Figs. 26, 27, 28 and 29 and quantitatively summarised in Table 16, comparing crashworthiness parameters for each design against the

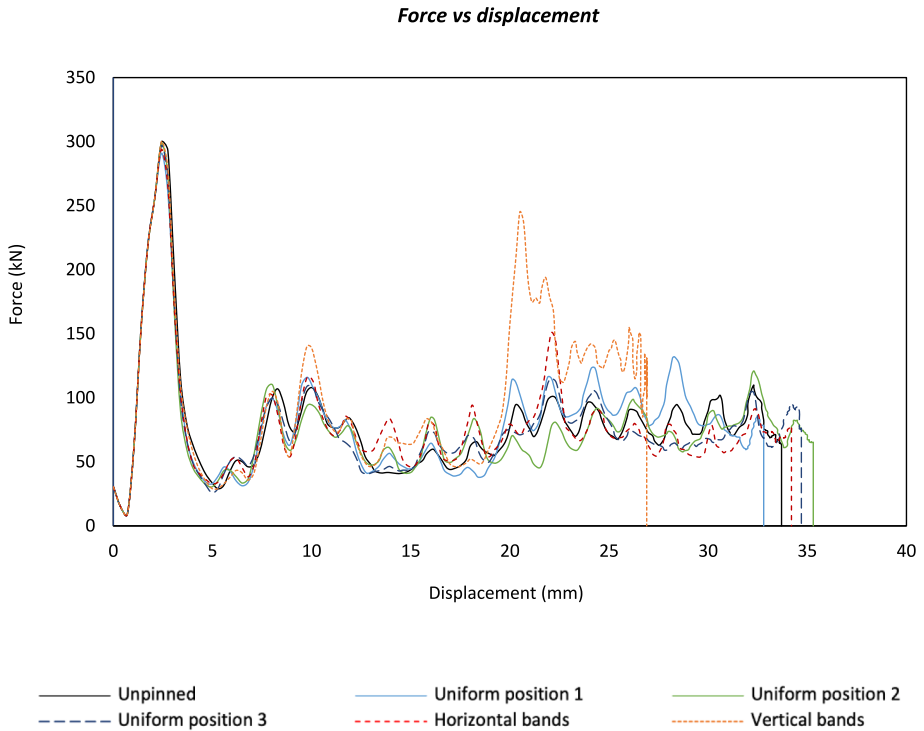


Fig. 25 Comparison of force–displacement for the different designs in their unpinned configuration, model 2

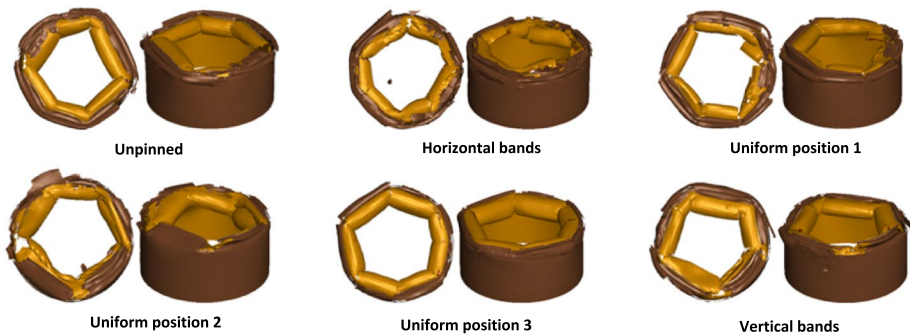


Fig. 26 Final crushed morphologies with z-pinning properties (I), model 1: left) top view, middle) bottom view, right) iso view

unpinned tube. In configuration (I), the 'Uniform position 2 (I)' and 'Vertical bands (I)' designs exhibit compromised petal formation during crushing and pronounced ply bending, as indicated by decreased mean forces (−8.7% and −6.5%, respectively). Conversely, 'Uniform position 3 (I)' demonstrates improved progressive crushing, enhanced petal formation, and reduced ply bending, confirmed by notable increases in mean force (+ 6.6%) and CFE (+

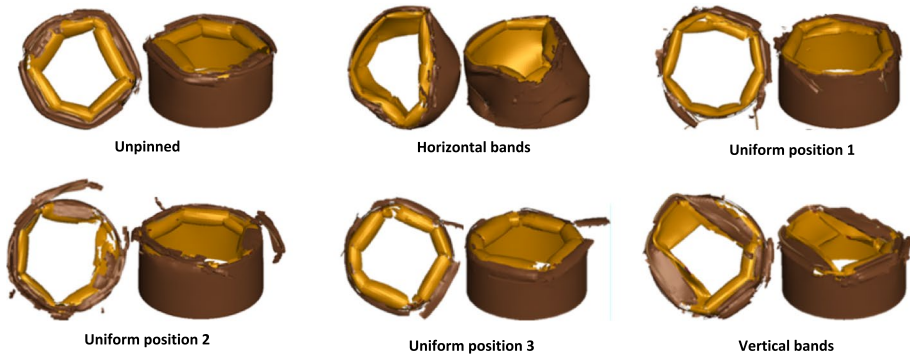


Fig. 27 Final crushed morphologies with z-pinning properties (II), model 1: left) top view, middle) bottom view, right) iso view

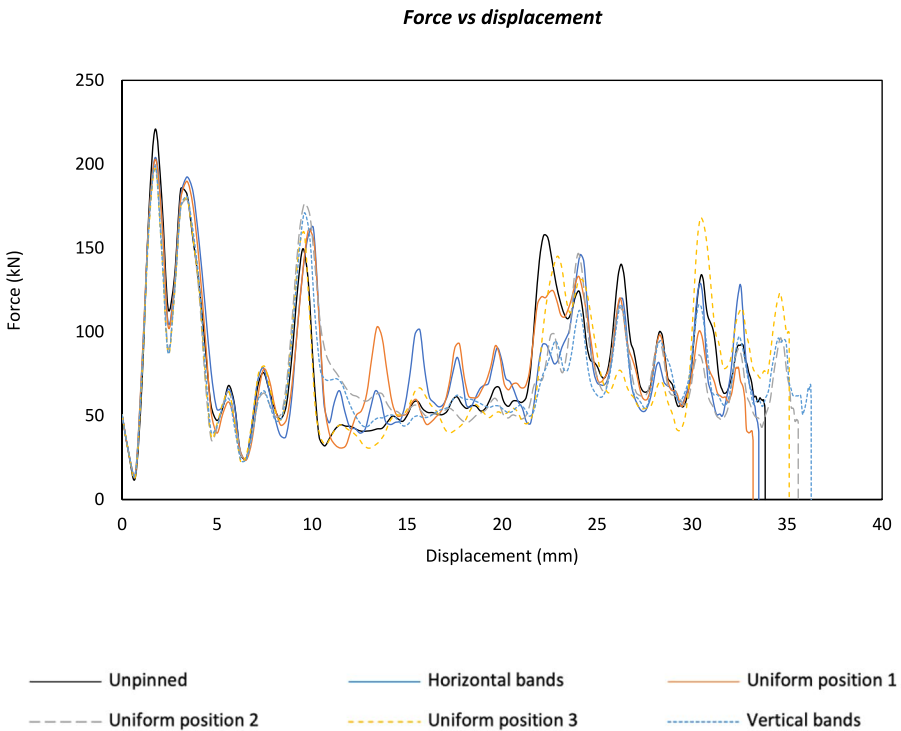


Fig. 28 Crashworthiness parameters evolution for different designs in configuration (I), model 1

19.3%), despite a decrease in peak force (−10.7%). The 'Horizontal bands (I)' and 'Uniform position 1 (I)' are the closest to the unpinned response, showing minimal variations in peak forces (−7.6% and −8.1% respectively), slightly altered maximum displacement (−0.9% and −1.8%, respectively), and minor increases in CFE (+ 6.5% and +3.5%, respectively). However, 'Horizontal bands (I)' and 'Uniform position 1 (I)' display consistent failure mecha-

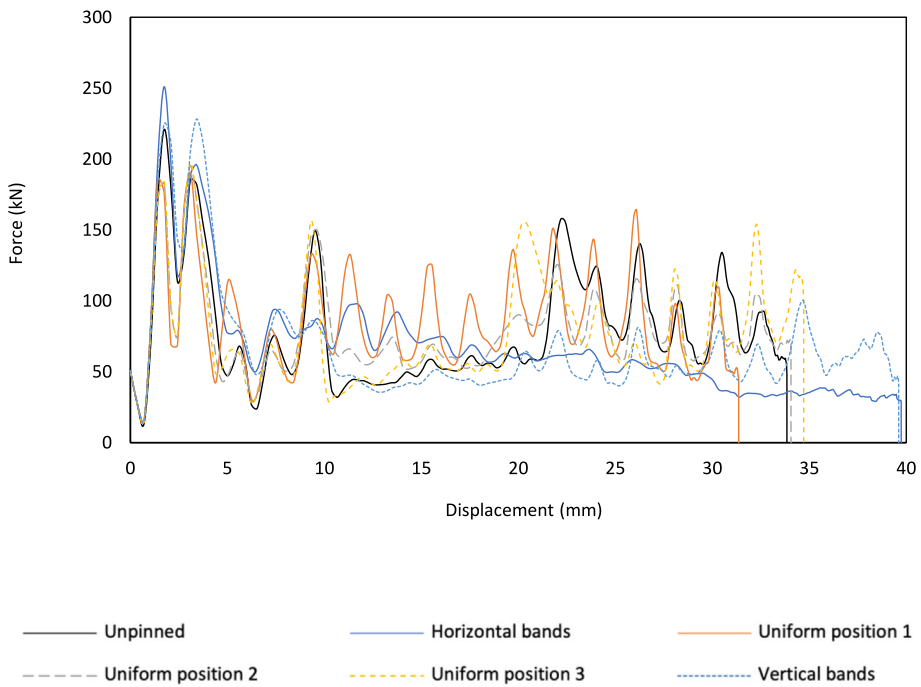
Force vs displacement

Fig. 29 Crashworthiness parameters evolutions for different designs in configuration (II), model 1

Table 16 Values of crashworthiness parameters for various designs

	$F_{max} (kN)$	$F_m (kN)$	$d (mm)$	$CFE(\%)$
Experimental	136 ± 6	78 ± 3	34 ± 1	58
Unpinned	221	77	34	35
H bands (I)	204	76	34	37
H bands (II)	251	51	40	20
Uniform 1 (I)	203	73	33	36
Uniform 2 (I)	200	70	36	35
Uniform 3 (I)	197	82	35	42
Uniform 1 (II)	191	78	31	41
Uniform 2 (II)	195	77	34	39
Uniform 3 (II)	197	83	35	42
V bands (I)	199	72	36	36
V bands (II)	228	62	40	27

nisms with reduced ply bending compared to the unpinned configuration.'Uniform position 3 (I)'experiences a less stable final compaction with a higher peak force in the final stage compared to other designs. In configuration (II), the'Horizontal bands (II)'design significantly reduces Mode-I dominance, triggering early delamination, pronounced ply bending, and catastrophic failure at the base between pinned regions due to stress concentration. These effects dramatically worsen performance, as reflected by a large increase in maxi-

imum displacement (+ 17.6%) and substantial reductions in mean force (−33.5%) and CFE (−41.5%). 'Vertical bands (II)' also show significant degradation, with substantial ply bending at the crushing front and a marked increase in displacement (+ 17.2%), alongside decreases in mean force (−19.2%) and CFE (−21.7%). 'Uniform position 1 (II)' maintains stable crushing behaviour, with a notable maximum displacement reduction (−7.2%) and improved CFE (+ 17%), while its mean force remains largely unchanged (+ 1.0%). 'Uniform position 3 (II)' offers moderate enhancements, particularly notable in terms of mean force (+ 7.3%) and CFE (+ 20%), with minimal displacement change (+ 2.7%). Finally, 'Uniform position 2 (II)' achieves meaningful crashworthiness improvements, particularly with increased CFE (+ 13.7%), reduced peak force (−11.7%), alongside a minor displacement increase (+ 0.8%). Overall, 'Uniform position 1 (II)' emerges prominently as the most balanced and effective design due to reduced displacement and peak force, stable crushing mechanisms with a slight increase in mean force, thus improving the CFE value. Some observations can be drawn, a higher G_{IC} with a suitably chosen initial unpinned length for petal-like shape initiation, as shown by 'Uniform position 1 (II)', enhances crashworthiness while a strong G_{IC} at the initial crash front increases ply bending. The choice of reinforcement strategy depends on application requirements, emphasising the need for multi-parameter evaluations in crashworthiness assessments (see Figs. 26, 27, 28, 29 and 30).

5.2 Crashworthiness Results from the Cohesive Model 2

As observed in Table 17 and Figs. 31, 32, 33 and 34, the 'Horizontal bands' design exhibits minor variations in maximum crushed displacement (−3.2% for (I), +4.0% for (II)) and peak

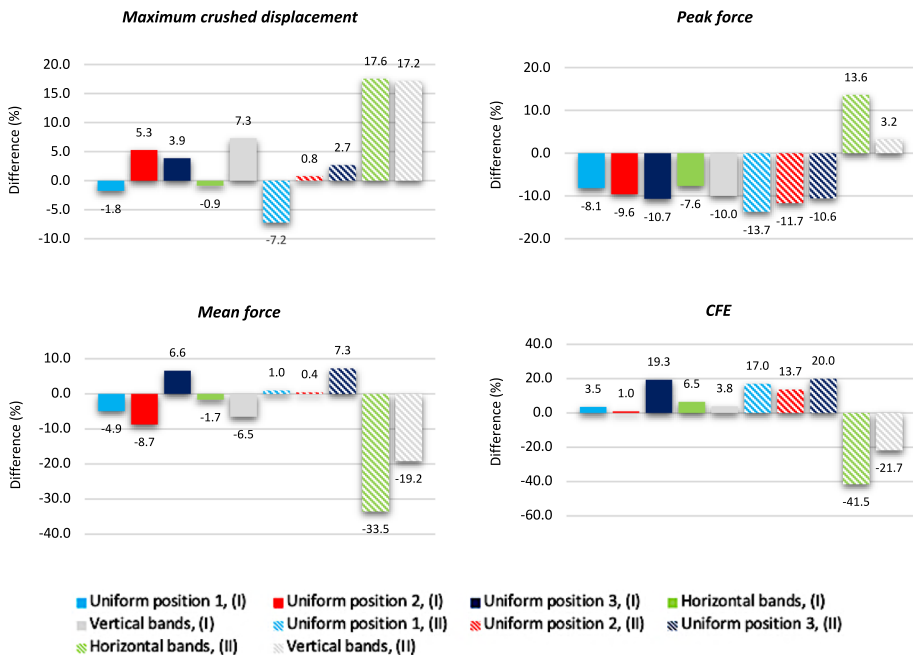
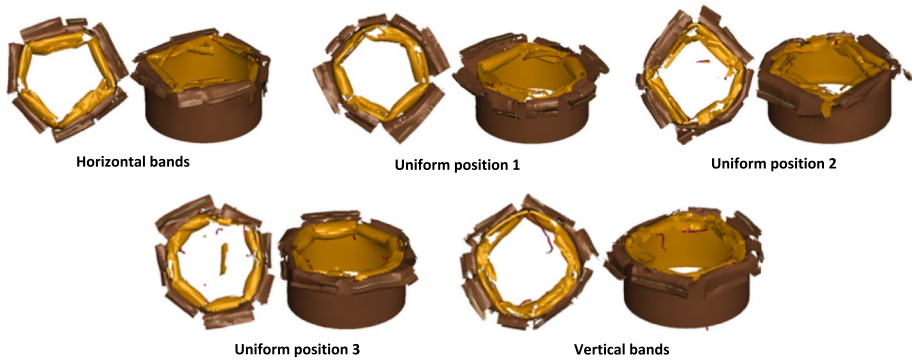


Fig. 30 Variations of crashworthiness factors from z-pinned reinforced designs compared to the unpinned configuration (model 1)

Table 17 Values of crashworthiness parameters for various designs

Configuration	F_{max} (kN)	F_m (kN)	d (mm)	CFE(%)
Experimental	136 ± 6	78 ± 3	34 ± 1	58
Unpinned	300	78	34	26
H bands U	294	75	34	26
H bands (I)	292	73	33	25
H bands (II)	298	70	36	24
Uniform 1 U	291	80	33	28
Uniform 1 (I)	297	67	36	23
Uniform 1 (II)	288	67	36	23
Uniform 2 U	299	74	35	25
Uniform 2 (I)	301	73	37	24
Uniform 2 (II)	299	80	32	27
Uniform 3 U	297	76	35	26
Uniform 3 (I)	297	78	33	26
Uniform 3 (II)	301	75	35	25
V bands U	300	109	27	36
V bands (I)	303	75	34	25
V bands (II)	303	77	34	26

**Fig. 31** Final crushed morphologies with z-pinning properties (I), model 2: left) top view, middle) bottom view, right) iso view

force (-0.7% in (I) and $+1.4\%$ in (II)) for both z-pinning configurations, accompanied by modest reductions in mean crushing force (-2.1% for (I), -6.2% for (II)). Uniform position 1 shows important reductions in mean crushing force (-16.6% for (I), -16.4% for (II)), significantly increased maximum crushing displacement ($+10.4\%$ for (I), $+10.1\%$ for (II)), and notable decreases in CFE (-18.3% for (I), -15.6% for (II)), suggesting less effective energy absorption. Uniform position 2 presents stable peak forces compared to the unpinned results ($+0.7\%$ in configuration (I) and 0% in configuration (II)). However, Uniform position 2 in configuration (II) increased mean force and CFE ($+8.4\%$), alongside a notable reduction in maximum displacement (-7.9%), highlighting its superior crashworthiness performance compared to configuration (I) of the same reinforced design. Conversely, configuration (I)

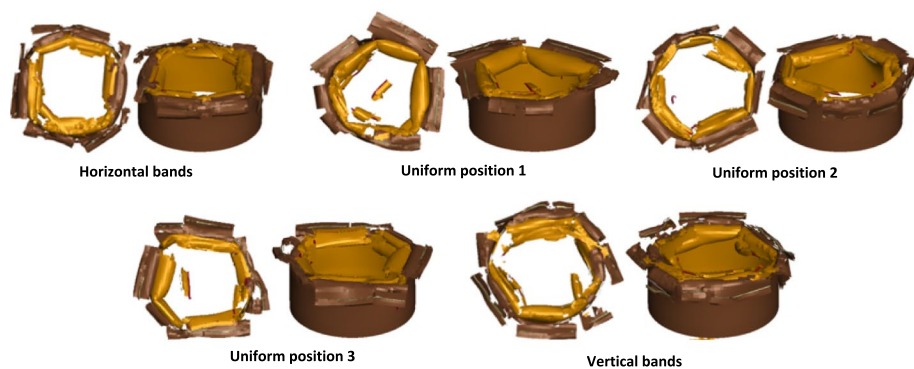


Fig. 32 Final crushed morphologies with z-pinning properties (II), model 2: left) top view, middle) bottom view, right) iso view

Force vs displacement

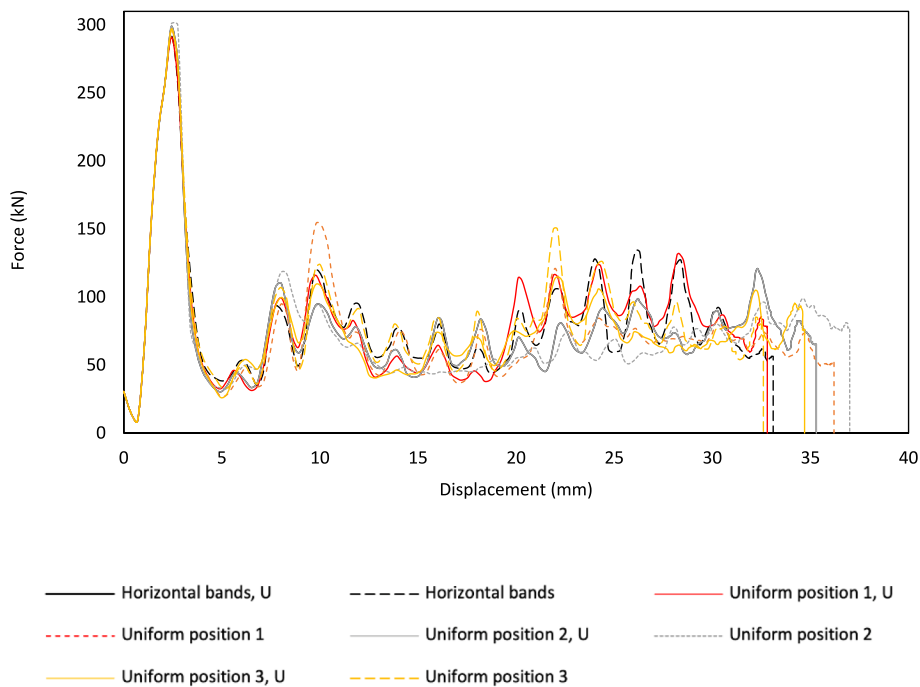


Fig. 33 Crashworthiness parameters evolutions for different designs in configuration (I), model 2

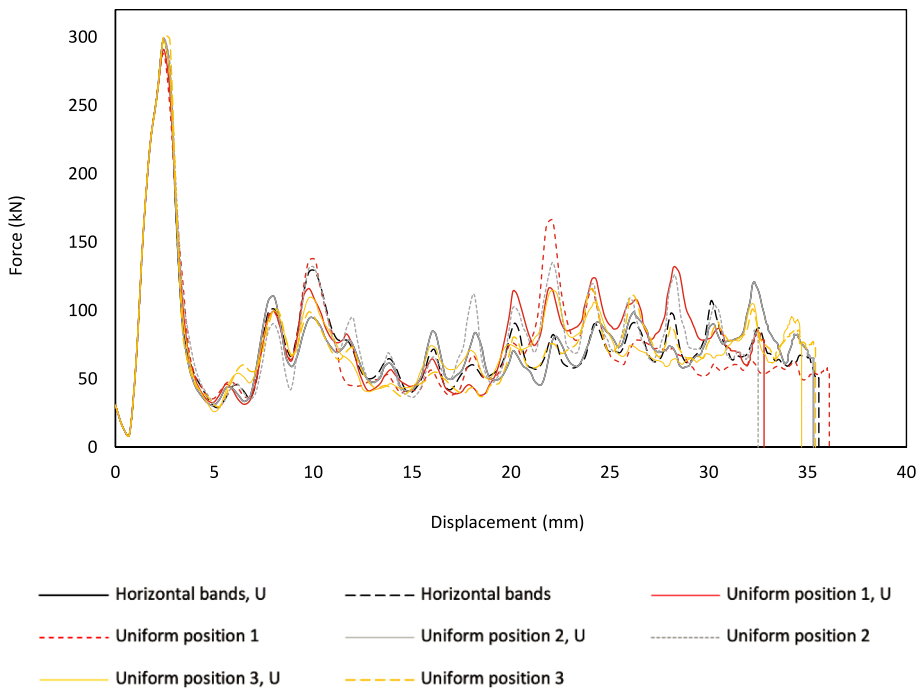
Force vs displacement

Fig. 34 Crashworthiness parameters evolutions for different designs in configuration (II), model 2

exhibits a negative decrease in mean crushing force (-1.9%) and CFE ($+2.6\%$), while experiencing a slight displacement increase ($+4.8\%$). Finally, 'Uniform position 3' configurations display negligible peak force changes (0%) and minor mean force variations ($+2.1\%$), with configuration (I), improving the CFE ($+2.1\%$). The maximum displacement was also reduced. While with z-pinning configuration (II), peak and mean force were increased and decreased, respectively, negatively impacting the CFE (-3%). Overall, 'Uniform position 3 (I)' appears promising for balanced performance, while 'Uniform position 2 (II)' emerges as the optimal design regarding crashworthiness, with significantly increased mean crushing force and CFE combined with a reduction in maximum displacement and no change in peak force compared to the unpinned results (see Figs. 31, 32, 33, 34 and 35).

5.3 Crashworthiness Results from Model 3

For model 3, despite both pinned and unpinned tubes absorbing the same total impact energy, the pinned tube showed increased Specific Energy Absorption (SEA), driven by a combination of reduced maximum crushed displacement (-4.6%). As illustrated in Table 18 and Figs. 36, 37 and 38, the pinned tube exhibited a slightly lower initial peak force (-1.5%) compared to the unpinned tube, followed by a comparable initial force drop. However, the pinned tube maintained a significantly higher mean crushing force ($+13.1\%$), leading to

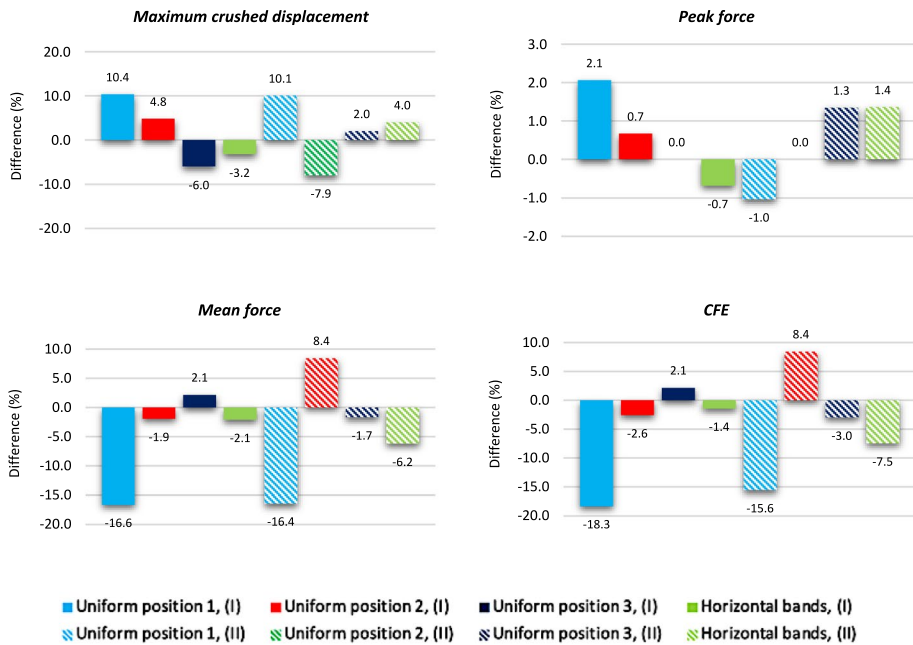


Fig. 35 Variations of crashworthiness factors from z-pinned reinforced designs compared to the unpinned configuration (model 2)

Table 18 Crashworthiness parameters between unpinned and pinned tubes, model 3

Z-pin diameter/Z-pin areal density	F_{max} (kN)	F_m (kN)	d (mm)	CFE(%)
Unpinned	218	65	35	30
0.51 mm/0.5%	215	73	33	34

a notably smaller final crushed displacement. Figure 38 demonstrates improved and more controlled crushing due to z-pinning reinforcement, despite an observed initiation of local buckling at the tube's bottom, which could be caused by a higher structural stiffness. Overall, the results indicate the benefits of this z-pinning configuration in improving crashworthiness performance. However, as mentioned previously, these results cannot draw a strong conclusion, as this model requires further calibration against the experimental data.

6 Conclusion

This study investigated the crashworthiness of z-pinned CFRP tubular energy absorbers through LS-DYNA finite element modelling, employing three numerical approaches to simulate interlaminar behaviour: the energy-based tiebreak contact method (Models 1 and 3) and the cohesive zone method (Model 2). The MAT54 material model was used to capture the failure mechanisms of the composite laminate, with Models 1 and 2 adopting an averaging method to account for z-pin bridging, while Model 3 explicitly modelled local pin behaviour. Experimental reference data was sourced from DCB tests (for 0.28 mm pins in

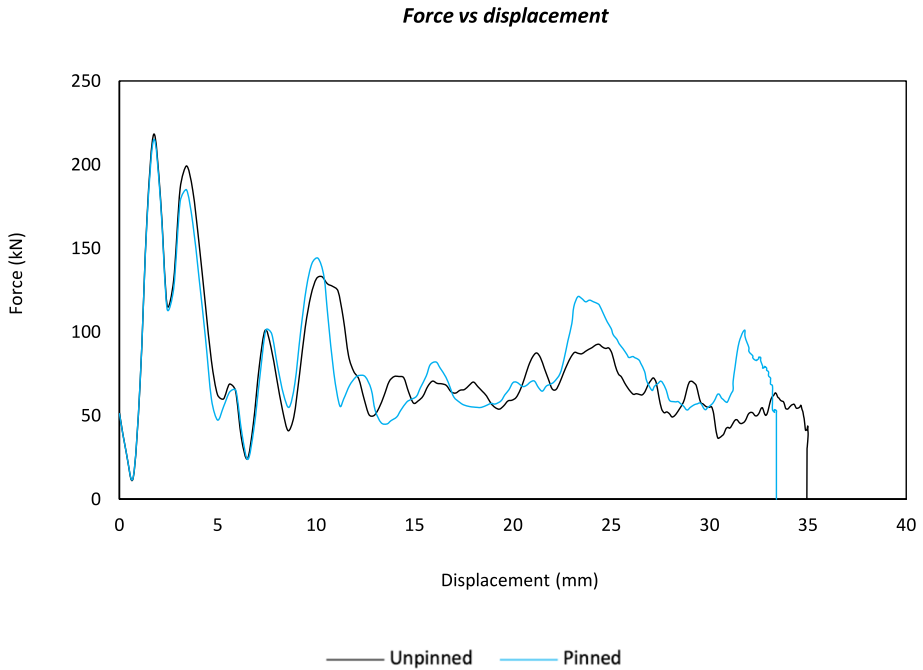


Fig. 36 Crashworthiness parameters evolutions, model 3

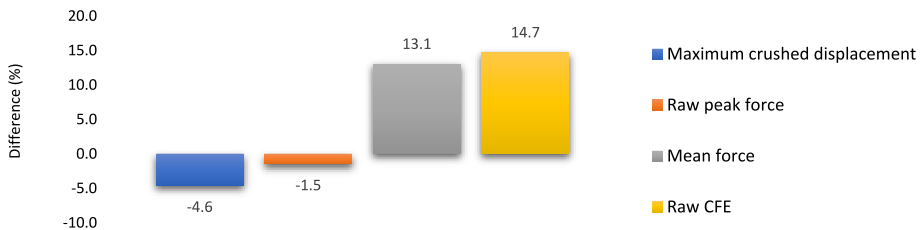


Fig. 37 Variations of crashworthiness factors from the pinned reinforced specimen compared to the unpinned configuration (model 3)

Models 1 and 2) and pull-out tests (for 0.51 mm pins in Model 3). Experimental validation was conducted for unpinned designs in all models, while Model 2 required selective validation due to computational limitations.

Numerical results demonstrated that z-pinning can significantly enhance crashworthiness by improving Specific Energy Absorption (SEA), reducing peak force, and increasing Crushing Force Efficiency (CFE), all while maintaining controlled progressive crushing. Model 1 results highlighted 'Uniform Position 1 (II)' and 'Uniform Position 2 (II)' as the most balanced and effective designs, achieving desired improvements in crashworthiness. These configurations benefited from an optimal balance of fracture toughness and unpinned length,

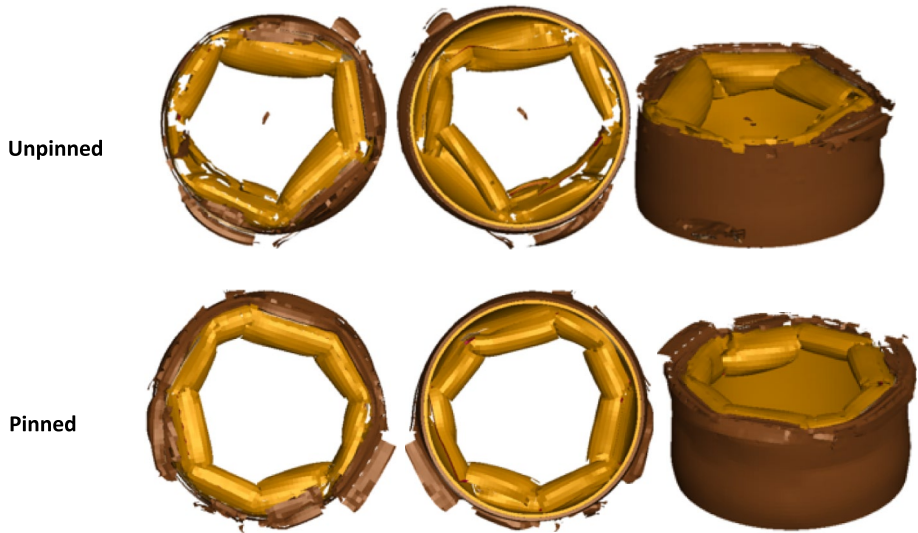


Fig. 38 Model 3 final deformation, (left) top, (middle) bottom, (right) iso view

favouring efficient petal formation with reduced ply bending. In Model 2, the 'Uniform Position 2 (II)' displayed superior crashworthiness due to a notable increase in mean crushing force and CFE, accompanied by reduced maximum displacement and stable peak force. Although Model 3 with individually modelled 0.51 mm z-pins at a 0.5% areal density initiated localized buckling at the base of the tube, attributed to higher structural stiffness, the overall crashworthiness improved due to enhanced energy absorption. However, Model 3 faced numerical instabilities at higher-density pinning or smaller (0.28 mm) pins, limiting its applicability to lower-density reinforcement configurations.

Further experimental validation of pinned specimens is essential to refine numerical predictions. Future research should prioritise optimising z-pinning configurations, evaluating strain-rate effects, and investigating alternative reinforcement materials to mitigate the inherent trade-off between out-of-plane reinforcement and in-plane property degradation. Additionally, more detailed modelling approaches, including ply-level delamination, comprehensive 3D failure analyses, and evaluations under off-axis loading conditions, should be pursued to achieve more realistic and accurate crashworthiness predictions. Specialised interaction models with bilinear and trilinear pull-out traction–displacement laws are also necessary to accurately capture z-pin bridging effects in future simulations. By systematically evaluating z-pinning reinforcement strategies, this study contributes to improving the energy absorption capabilities of CFRP crash absorbers, providing a foundation for more impact-resistant and lightweight structural applications in aerospace, automotive, and high-performance engineering sectors.

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Data Availability No datasets were generated or analysed during the current study.

Declarations

Institutional Review Board N/A.

Informed Consent N/A.

Competing interests The authors declare no competing interests.

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