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Comparative Assessment of Chang–Chang and Tsai–Wu Failure Criteria for Predicting Low-Velocity Impact Damage in CFRP Laminates using LS-DYNA

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Abstract

Low velocity Impact (LVI) -arising from dropped tools, runway debris, or accidental strikes during manufacturing and in-service handling -remains a governing design driver for carbon-fibre-reinforced polymer (CFRP) structures. The resulting sub-surface damage frequently manifests as barely visible impact damage (BVID) that nevertheless degrades residual strength, stiffness, and fatigue life. This work presents a FEA investigation of single LVI impact events on CFRP- laminates using the explicit non-linear solver LS-DYNA. A shell-based, ply-discretised model of a $[-45/0/45/90]_{3s}$, 24-ply CFRP plate impacted by a rigid hemispherical steel indenter (25.4 mm Φ , 1.85 kg) was developed in LS-PrePost and assigned the *MAT_054/055 Enhanced Composite Damage material model. The benchmark configuration (40 J impact energy, 6.5 m/s impact velocity) was validated against the experimental and numerical data of Heimbs, achieving 0% error in peak contact force and 2.63% error in absorbed energy. Four parametric case studies were subsequently executed to quantify the influence of laminate thickness (5 mm and 7 mm) and impact energy (10 J, 15 J and 20 J) on the impact response, each evaluated separately under the Chang–Chang and Tsai-Wu failure criteria. Peak contact force & absorbed plate energy were found to scale monotonically with both laminate thickness and impact energy, whereas the residual impactor velocity and peak displacement were governed principally by the flexural stiffness & energy-dissipation capacity of the laminate. The two failure criteria yielded near-identical global force energy histories, indicating that the divergence between the Chang-Chang and Tsai-Wu formulations operate

chiefly at the level of local matrix/fibre damage rather than the bulk structural response -a finding consistent with recent comparative assessments of ply-level failure theories .The validated model and the accompanying parametric dataset provide a computationally economical basis for predicting BVID extent and for supporting impact-tolerant design of CFRP structures in aerospace and automotive applications.

Keywords CFRP- laminates , LVI(LVI) , LS-DYNA, FEA, Chang–Chang criterion ,Tsai–Wu criterion, Barely visible impact damage (BVID) , Ply-level failure

1. Introduction

Composite materials are engineered systems formed by combining two or more constituents with distinct physical or chemical properties in order to obtain performance characteristics unattainable by any single constituent alone. In fibre-reinforced polymer (FRP) composites, a matrix phase -conventionally a thermosetting resin such as epoxy or polyester, although thermoplastic, metallic and ceramic matrices are also employed -binds and stabilises a reinforcing phase of continuous or discontinuous fibres, most commonly carbon, glass, or aramid. This architecture yields exceptionally high specific strength and specific stiffness, tailorable anisotropic behaviour, corrosion resistance, and thermal stability, and has driven the widespread adoption of composites in aerospace fuselage and wing structures, automotive body panels, wind-turbine blades, marine hulls, civil infrastructure, and sporting goods.

A recurring design concern for laminated composite structures is their sensitivity to transverse impact loading. Under LVI (generally below approximately 10 m/s), visible surface indentation is often negligible even though sub-surface matrix cracking, delamination, and localised fibre breakage develop beneath the impact site -a condition widely termed barely visible impact damage (BVID). Because BVID evades routine visual inspection, it poses a disproportionate threat to structural integrity: residual load-carrying capacity, fatigue resistance, and damage-tolerance margins can all be compromised without any external indication of distress. Impact threats of practical concern range from low-energy incidents (dropped tools, hail, bird strikes) to high-energy ballistic and debris impacts, as well as repeated low-energy impacts that accumulate fatigue damage over the service life of the structure. The dominant modes of damage - matrix cracking, fibre/matrix debonding, delamination, and fibre breakage, and fibre breakage -interact with laminate lay-up, constituent material properties, impactor geometry and energy, and environmental factors such as moisture and temperature, all of which govern the size, shape, and severity of the resulting damage zone.

Established non-destructive evaluation (NDE) techniques -ultrasonic C-scanning, infrared thermography, X-ray radiography, and acoustic emission monitoring -together with instrumented drop-weight and ballistic impact testing, remain the principal means of experimentally characterising impact damage. However, these methods are costly and time-consuming when applied across the large design space typically required for parametric studies. Explicit FE (FE) simulation therefore plays an increasingly central role in impact-tolerant design, enabling the effects of laminate thickness, stacking sequence, impactor energy, and ply-level failure criterion to be explored efficiently prior to physical testing.

Damage accumulation in impacted laminates has direct structural consequences. Fibre breakage and delamination reduce load-bearing capacity, matrix cracking lowers local stiffness and increases susceptibility to further deformation, and damage accumulated under cyclic loading accelerates fatigue degradation. Delamination fronts, once initiated, tend to propagate under subsequent mechanical or thermal cycling and, in layered structures, this propagation can culminate in catastrophic failure. Impact-induced surface irregularities can additionally disturb boundary-layer aerodynamics in aerospace and automotive applications, while cracked or delaminated regions provide ingress paths for moisture that further degrades the matrix through hydrolysis and swelling. Predicting the location, shape, and extent of ply-level damage -rather than only the global force-time response -is therefore essential to a design methodology capable of certifying impact-tolerant composite structures.

1.1 Literature Review

The theoretical foundations of ply-level failure prediction in composite laminates trace back to two classical formulations that remain in routine use within commercial explicit FE codes. Chang and Chang [1] proposed a progressive damage model that decouples fibre-dominated and matrix-dominated failure into separate, stress-based mode-specific expressions for tensile and compressive loading, allowing the mode of failure (fibre versus matrix) to be identified explicitly at each integration point. Tsai and Wu [2], by contrast, formulated a single quadratic interactive failure envelope in stress space that combines all in-plane stress components into one polynomial criterion; while computationally compact, this formulation does not natively distinguish between fibre- and matrix-dominated failure modes. Both criteria are implemented within the *MAT_054/055 Enhanced Composite Damage material model in LS-DYNA and remain the two most widely adopted ply-failure formulations for impact simulation of laminated composites, alongside the later Hashin and Puck criteria.

Heimbs et al. [3] examined LVI on CFRP plates under compressive preload through combined experimental & numerical modelling using LS-DYNA, and their reported force–time history and absorbed-energy data constitute a widely used and frequently re-used benchmark dataset for validating explicit FE impact models; the present study adopts this dataset as its principal validation reference. More recent contributions have continued to refine and extend this benchmark class of problem. Boukar et al. [4] developed a FEA model of LVI on biaxial GFR laminates and highlighted the sensitivity of predicted contact force and damage extent to element formulation and contact-definition parameters -a sensitivity that is directly relevant to the mesh and contact settings adopted in the present shell-based model. In their study on the dynamic progressive failure of thin CFRP laminated plates subjected to LVIs, Rezasefat et al. [5] showed that damage models which have been calibrated using single impact test results could be applied to predict the damage evolution due to repeated loads. In another work, Rezasefat et al. [6] utilized both experimental and numerical studies to investigate the effect of pre-existing impact damage on the LVI performance of CFRP- laminates, highlighting the relevance of the damage state dependent modelling in the case of real life components which are prone to BVID damage history.

On the specific question of failure-criterion selection, Chen et al. [7] proposed a rationalised improvement of the Tsai-Wu criterion that reconstructs the interactive strength coefficient from basic ply strength values and demonstrates improved predictive accuracy relative to the classical formulation under multiaxial stress states, particularly for triaxial loading; this line of work indicates that the discriminating power of interactive criteria such as Tsai–Wu can be meaningfully improved without abandoning their computational simplicity. The FE model by Xie et al. [8], validated using experiments, was proposed to study the LVI response of prestressed CFRP laminate under tensile and compressive preload, which is an improvement of the preloading technique suggested by Heimbs with different magnitudes of preload and impact velocities. . Fang et al. [9] examined the damage characteristics of CFRP- laminates subjected to multiple LVIs through combined testing and numerical simulation, reporting how the position and energy of successive impacts govern cumulative damage evolution. Xiao et al. [10] studied the residual compressive mechanical properties of composite stiffened panels following LVI using quantitative damage simulation, linking impact-induced ply damage to post-impact structural performance metrics of direct relevance to compression-after-impact (CAI) design allowables. In a further contribution, Xiao et al. [11] explored the impact response and damage mechanism of discontinuous CFRP- laminates under LVI, reporting how ply

discontinuities modify the local stress concentration and damage morphology relative to conventional continuous-ply lay-ups.

From a modelling-methodology perspective, Thorsson et al. [12] established a continuum shell-based approach for predicting the LVI response of composite laminates, benchmarking through-thickness stress-resultant accuracy against fully three-dimensional solid-element models and reporting comparable predictive fidelity at substantially reduced computational cost -a finding of direct relevance to the shell-element modelling strategy adopted in the present study. More recently, Ye et al. [13] revisited the Tsai–Wu failure criterion in the context of orthotropic constitutive modelling of carbon-fibre-reinforced PEEK thermoplastic composites, illustrating the continued relevance of the criterion beyond conventional thermoset CFRP systems. Sun et al. [14] inspected the oblique LVI response of carbon-epoxy composite laminates using a plastic continuum damage model coupled with cohesive interface elements for delamination, reporting that impact angle systematically alters both the peak contact force and the resulting damage footprint relative to normal-incidence impact.

Collectively, this body of work establishes three points of consensus that motivate the present study. First, explicit FE simulation, when properly validated against experimental force–time and energy data such as that of Heimbs et al. [3], can reliably reproduce the global impact response of CFRP- laminates [4,5,12]. Second, laminate architecture, preload state, prior damage state, and impact multiplicity all strongly influence damage extent and post-impact residual performance [5,6,8,9,10]. Third, the choice of ply-level failure criterion -and specifically the trade-off between the mode-separated Chang–Chang formulation [1] and the single-envelope Tsai–Wu formulation [2,7,13] -is a modelling decision whose influence on the global structural response, as distinct from the local ply-level damage prediction, has received comparatively limited systematic attention within a single validated FE framework. The present study addresses this gap by evaluating the Chang–Chang and Tsai–Wu criteria side-by-side, within one validated LS-DYNA model, across a controlled parametric matrix of laminate thickness and impact energy.

1.2 Research Gap and Novelty

While the individual influences of laminate thickness, impact energy, preload, and repeated impact on CFRP impact response have each been investigated separately in the literature reviewed above [4–14], comparatively few studies isolate the effect of the ply-level failure criterion itself -Chang–Chang versus Tsai–Wu -while holding the underlying elastic-damage

material model, mesh, contact formulation, and loading conditions fixed. This decoupling is necessary to determine whether observed differences in predicted impact response arise from the failure criterion or from confounding differences in model set-up between studies. The present work addresses this specific gap by conducting a controlled, single-framework comparison across four parametric case studies (Table 4), building on a benchmark model independently validated to within 2.63% of experimental data [3].

1.3 Aim and Objectives

This study aims at investigating the LVI response and ply failure characteristics of the CFRP laminates through the use of explicit FE modeling in LS-DYNA. Comparison will be made between the predictive ability of the Chang-Chang [1] and Tsai-Wu [2] failure criteria based on different values of laminate thickness and impact energy. Specifically, the aims of this study are:

- identify suitable benchmark literature providing experimental LVI data for CFRP-laminates;
- extract the geometric and material modelling details required to reconstruct the benchmark configuration;
- develop a shell-based FE model of a flat composite laminate (300×150 mm effective test area) in LS-DYNA;
- simulate single LVI for laminate thicknesses of 3 mm, 5 mm and 7 mm with $[0/90]_{2s}$ (24-ply) and $[-45/0/45/90]_{3s}$ lay-ups;
- quantify the impact response in terms of contact force–time, displacement–time, velocity–time and plate-energy history;
- evaluate the effect of laminate thickness and impact energy on damage initiation at the impact location; and
- validate the predicted impact response and ply-level damage footprint against the experimental benchmark data of Heimbs et al. [3].

The remaining sections of this article are structured as follows. Section 2 is devoted to an introduction to the FE method, material modeling, boundary conditions and parametric design of the four parametric case studies. Section 3 provides the findings from the validation of the benchmark model along with the outcomes of the parametric case studies, and compares the

Chang-Chang criterion with the Tsai-Wu criterion. Section 4 offers the main conclusions, while Section 5 suggests further research directions.

2. Materials and Methods

2.1 Overview of Methodology

The overall workflow adopted in this study, summarised in Fig. 1, proceeds from literature-based benchmark identification through geometric and FE model development, explicit dynamic simulation, and validation against experimental data, before the validated model is exercised across the parametric case-study matrix.

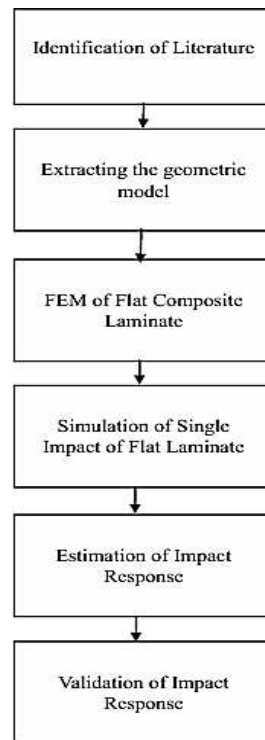


Fig. 1. Overall methodology adopted for the FE investigation

Literature identification and benchmark selection constitute the first stage, in which the study of Heimbs et al. [3] was reviewed to establish geometry, material properties, and validated impact-response data. The geometric model was then constructed and refined in LS-PrePost, removing features that do not materially affect the simulation while preserving ply-by-ply resolution. The resulting geometry was discretised into FEs, with shell elements used for the thin laminate plies and a rigid solid element used for the impactor, meshed more finely in the impact zone to resolve localised contact and damage gradients. The finalised model was exported in LS-DYNA keyword format (*.k) prior to solution and post-processing.

2.2 Benchmark Problem Definition

The benchmark configuration follows the experimental set-up of Heimbs et al. [3]. The CFRP plate has an effective (post-clamping) planar dimension of 300×150 mm and a nominal thickness of 2.7 mm, laid up as $[-45/0/45/90]_{3s}$ (24 plies). The plate is clamped with fixed support along its longitudinal edges and simple support along its lateral edges (Fig. 2). The impactor is a hemispherical steel indenter of 25.4 mm (1 in.) diameter and 1.85 kg mass. A reference impact energy of 40 J was selected, corresponding, for the fixed impactor mass, to an initial impact velocity of 6.5 m/s.

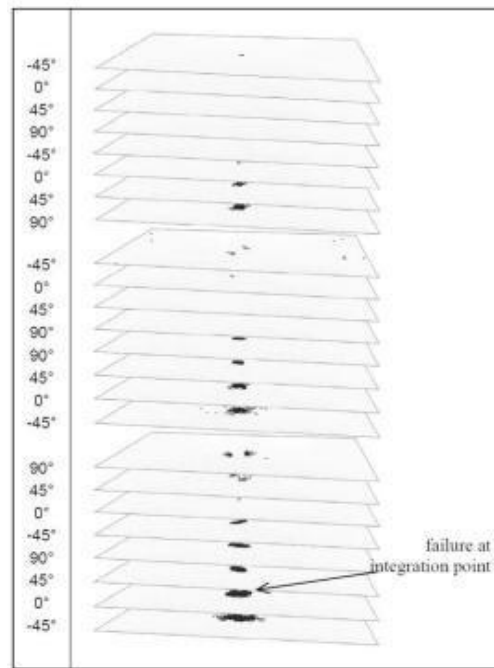


Fig. 2. Ply stacking orientation of the benchmark CFRP laminate.

The laminate was modelled using 4-noded Belytschko–Tsay shell elements, generated through the LS-PrePost shape-mesher (FEM → Element & Mesh → Shape Mesher → 4N Shell), which is computationally efficient for thin-walled structures in which through-thickness stress gradients are small (Fig. 3). The Belytschko–Tsay formulation was selected over a fully integrated shell in view of the substantially lower per-element computational cost in explicit time integration, at the accepted cost of requiring hourglass control (Section 2.3.3) to suppress spurious zero-energy deformation modes. The impactor, whose deformation is negligible relative to that of the laminate, was represented as a rigid solid sphere defined by its radius and density (FEM → Element & Mesh → Shape Mesher → Sphere Solid, Fig. 4).

Shape Mesher

Entity: 4N Shell

☒ P1 300,150,0

☐ P2 -300,150,0

☐ P3 -300,-150,0

☐ P4 300,-150,0

NxNo. 24 1

NyNo. 24 1

Target Name Plate

Target Part ID 1

Start Element ID 1001

Start Node ID 1001

Create Reject Accept

Done

Fig. 3. Shell discretization of the CFRP laminate.

Shape Mesher

Entity: Sphere Solid

Radius 12.7 1

Density 5 1

Center[Position]

x 0 y 0 z 20

Direct1 Direct2

dx 1 dx 0

dy 0 dy 1

dz 0 dz 0

Target Name Ball

Target Part ID 2

Start Element ID 2452

Start Node ID 2603

Create Reject Accept

Done

Fig. 4. Solid discretization of the rigid impactor.

2.3.1 Material Modelling

The laminate was assigned the *MAT_054/055 Enhanced Composite Damage material model (FEM → Keyword Manager → MAT → 054/055), which implements progressive ply-level damage evolution together with a user-selectable failure criterion -either Chang–Chang [1] or Tsai–Wu [2]. The impactor was modelled with *MAT_020 (Rigid; FEM → Keyword Manager → MAT → 020), consistent with its negligible deformation relative to the laminate. The ply-level orthotropic properties used for the CFRP laminate and the isotropic properties used for the steel impactor are summarised in Tables 1 and 2, respectively.

Keyword Input Form

NewID MatDB RefBy Pick Add Accept Delete Default Done

☐ Use *Parameter ☐ Comment (Subsys: 1 Bench-mark_1shell_energy_13.4.k) Setting

*MAT_ENHANCED_COMPOSITE_DAMAGE (TITLE) (054/055) (1)

TITLE plate_mat

1	MID	RQ	EA	EB	EC	PRBA	PRCA	PRCB
	1	1.600e-06	153.000000	10.300000	0.0	0.24000000	0.0	0.0
2	GAB	GBC	GCA	KE	AOPT	ZWAY	TI	
	5.19999998	5.19999998	5.19999998	0.0	0.000			
3	XP	YP	ZP	A1	A2	A3	MANGLE	
	0.0	0.0	0.0	0.0	0.0	0.0		
4	V1	V2	V3	D1	D2	D3	DFAILM	DFAILS
	0.0	0.0	0.0	0.0	0.0	0.0	0.0	0.0
5	TFAIL	ALPH	SOFT	FBRT	YCFAC	DFAILT	DFAILC	EFS
	0.0	0.0	1.00000000	0.0	2.00000000	0.0	0.0	0.0
6	XC	XI	YC	YI	SC	CRIT	BETA	
	1.50000000	2.54000000	0.23600000	0.08200000	0.09000000	54.0	0.0	

Fig. 5. Assignment of MAT_054/055 to the CFRP laminate.

Keyword Input Form

NewID MatDB RefBy Pick Add Accept Delete Default Done

☐ Use *Parameter ☐ Comment (Subsys: 1 Bench-mark_1shell_energy_13.4.k) Setting

*MAT_RIGID (TITLE) (020) (1)

TITLE ball_solid

1	MID	RQ	E	PR	N	COUPLE	M	ALIAS
	1	2.159e-04	210.000000	0.30000000	0.0	0	0.0	
2	CMQ	CON1	CON2					
	0.0	0	0					
3	LCO	OR	A1	A2	A3	V1	V2	V3
	0.000	0.0	0.0	0.0	0.0	0.0	0.0	0.0

COMMENT:

Fig. 6. Assignment of MAT_020 (rigid) to the impactor.

Table 1 Material properties of the steel impactor (*MAT_020)

Property	LS-DYNA parameter	Value
Mass density	RO	$2.156 \times 10^{-4} \text{ kg/mm}^3$
Young's modulus	E	210 GPa
Poisson's ratio	PR	0.3
Impactor radius	—	12.7 mm

Table 2 Ply-level orthotropic material properties of the CFRP laminate (*MAT_054/055)

Property	LS-DYNA parameter	Value
Mass density	RO	1.6 g/cm ³
Longitudinal Young's modulus	EA	155 GPa
Transverse Young's modulus	EB	10.3 GPa
Major Poisson's ratio	PRBA	0.3
Shear modulus	GAB	5.2 GPa
Maximum matrix strain (tension & compression)	DFAILM	0.1
Maximum shear strain	DFAILS	0.03
Maximum fibre tensile strain	DFAILT	0.017
Maximum fibre compressive strain	DFAILC	-0.0135
Longitudinal compressive strength	XC	1.5 GPa
Longitudinal tensile strength	XT	2.54 GPa
Transverse compressive strength	YC	0.233 GPa
Transverse tensile strength	YT	0.082 GPa
Shear strength	SC	0.090 GPa

2.3.2 Boundary Conditions & Contact

All four edges of the CFRP plate were clamped by applying single-point constraints (FEM → Keyword Manager → Boundary → SPC Set → Create), restricting all translational and rotational degrees of freedom along the clamped boundary. The impactor was assigned an initial velocity of $v_r = -6.5 \text{ m/s}$ normal to the plate surface (FEM → Keyword Manager → Initial Velocity → Add, Fig. 7), consistent with the 40 J benchmark energy. Contact between the impactor and the laminate was defined using contact automatic surface to surface (FEM → Keyword Manager → Contact → Automatic Surface-to-Surface → Add, Fig. 8), which was selected for its robustness in handling the large local curvature of the hemispherical indenter and the finite sliding that develops between the impactor and the deforming shell surface.

Fig. 7. Definition of the impactor's initial velocity.

Fig. 8. Definition of the automatic surface-to-surface contact.

Fig. 9. Section definition for the solid impactor.

Fig. 10. Ply-by-ply orientation assignment (24 plies).

Fig. 11. Assignment of section and material IDs to each laminate part.

2.3.3 Solution Controls

The explicit dynamics solution scheme was selected as appropriate to the high strain-rate, large-deformation nature of impact events, together with the solver control keywords summarised in Table 3.

Table 3 LS-DYNA solution control keywords

Control keyword	Purpose	Key parameters
*CONTROL_TIMESTEP	Governs the explicit time-step size for numerical stability.	DTMIN – minimum permissible time step; TSSFAC – time-step scaling factor; TSLIMIT – adaptive time-step threshold.
*CONTROL_ENERGY	Manages the output and tracking of energy components for model validation.	HGEN – hourglass-energy toggle; IGNORE – selective exclusion of energy terms.
*CONTROL_TERMINATION	Specifies the simulation end time.	ENDTIM = 10 ms.
*CONTROL_CONTACT	Governs contact-algorithm parameters.	SOFT – contact stiffness scaling; IGNOR – contact exclusion options.
*CONTROL_HOURLASS	Suppresses non-physical (hourglass) deformation modes in under-integrated elements.	HGFORM – hourglass control formulation; QH – hourglass energy scaling factor.
*CONTROL_SHELL	Governs shell-element-specific parameters.	NIP – through-thickness integration points; ESORT – shell element sorting.

Output was requested through *DATABASE_BINARY_D3PLOT (Fig. 12) at defined time intervals for full-field post-processing, together with ASCII history files (GLSTAT, MATSUM, NCFORC, NODFOR, NODOUT, RCFORC, SLEOUT, SPCFORC) for extraction of the force–time, energy, displacement, and velocity histories reported in Section 3.

Fig. 12. DATABASE_BINARY_D3PLOT output configuration.

2.4 Ply-Level Failure Criteria

Two ply-level failure criteria available within the *MAT_054/055 formulation were evaluated in this study: the Chang–Chang criterion [1] and the Tsai–Wu criterion [2]. Both operate on the in-plane stress components (σ_{11} , σ_{22} , σ_{12}) referred to the ply material axes.

$$\text{Chang–Chang -fibre tensile failure } (\sigma_{11} > 0): (\sigma_{11}/X_t)^2 + \beta(\sigma_{12}/S)^2 \geq 1 \quad (1)$$

$$\text{Chang–Chang -fibre compressive failure } (\sigma_{11} < 0): (\sigma_{11}/X_c)^2 \geq 1 \quad (2)$$

$$\text{Chang–Chang -matrix tensile/shear failure } (\sigma_{22} > 0): (\sigma_{22}/Y_t)^2 + (\sigma_{12}/S)^2 \geq 1 \quad (3)$$

$$\text{Chang–Chang -matrix compressive failure } (\sigma_{22} < 0): (\sigma_{22}/2S)^2 + [(Y^c/2S)^2 - 1](\sigma_{22}/Y^c) + (\sigma_{12}/S)^2 \geq 1 \quad (4)$$

where X_t , X^c , Y_t , Y^c and S are the longitudinal tensile, longitudinal compressive, transverse tensile, transverse compressive, and shear strengths given in Table 2, and β is a shear-contribution weighting factor. The Tsai–Wu criterion [2], by contrast, formulates a single quadratic failure envelope in stress space:

$$F_1\sigma_1 + F_2\sigma_2 + F_{11}\sigma_1^2 + F_{22}\sigma_2^2 + F_{66}\sigma_6^2 + 2F_{12}\sigma_1\sigma_2 \geq 1 \quad (5)$$

where $F_1 = 1/X_t - 1/X^c$, $F_2 = 1/Y_t - 1/Y^c$, $F_{11} = 1/(X_t X^c)$, $F_{22} = 1/(Y_t Y^c)$, $F_{66} = 1/S^2$, and F_{12} is an interaction term. The rationalisation of this interaction coefficient from basic strength values, rather than from an additional biaxial test, has itself been the subject of recent methodological refinement [7,13]. The key distinction between the two formulations is that the Chang–Chang criterion decouples fibre-dominated and matrix-dominated failure modes into separate, mode-specific expressions (Eqs. 1–4), whereas the Tsai–Wu criterion combines all in-plane stress components into a single interactive polynomial envelope (Eq. 5) without explicit mode separation. This distinction is expected to influence the predicted damage morphology (mode, shape, and extent) more strongly than the global force–time or energy response -a hypothesis examined in Section 3.6.

2.5 Design of Parametric Case Studies

Building on the validated benchmark model, four parametric case studies were designed to isolate the effects of laminate thickness and impact energy under each of the two failure criteria (Table 4).

Table 4 Design matrix of the four parametric case studies

Case study	Failure criterion	Variable parameter	Levels investigated
I	Chang–Chang	Laminate thickness	5 mm, 7 mm
II	Chang–Chang	Impact energy	10 J, 15 J, 20 J
III	Tsai–Wu	Laminate thickness	5 mm, 7 mm
IV	Tsai–Wu	Impact energy	10 J, 15 J, 20 J

In each case, the resultant contact force, absorbed plate energy, impactor velocity, and impactor displacement were extracted and compared, providing a consistent basis for evaluating the influence of thickness and impact energy under each failure criterion, and for the cross-criterion comparison presented in Section 3.6.

3. Results and Discussion

3.1 Benchmark Validation

The benchmark model (2.7 mm thickness, 40 J / 6.5 m/s impact) was first exercised in isolation to establish the baseline impact response, before being validated against the experimental and numerical results reported by Heimbs et al. [3].

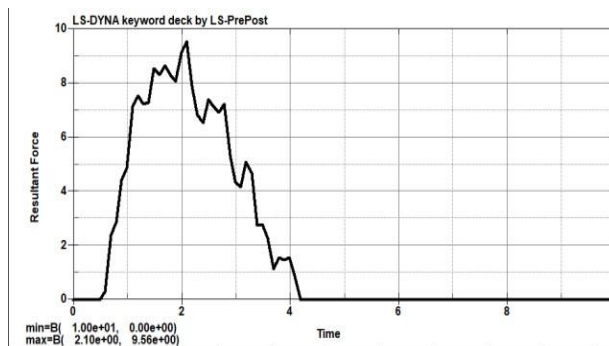


Fig. 13. Predicted reaction force versus time, benchmark model.

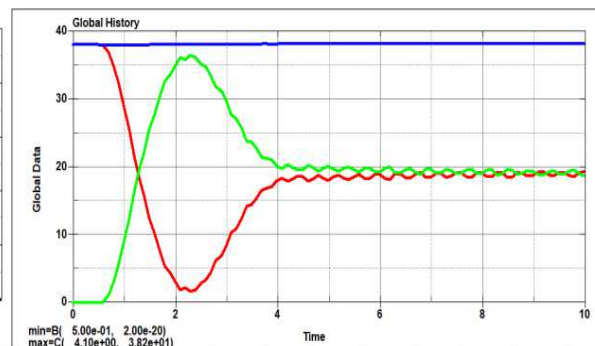


Fig. 14. Predicted plate energy history, benchmark model.

The predicted reaction force rises to a peak of 9.8 N (Fig. 13) before decaying as the impactor rebounds, and the energy history (Fig. 14) shows a total of 37 J transferred between the impactor and the plate, with the kinetic energy lost by the impactor closely matching the potential/strain energy gained by the plate, confirming energy conservation within the model.

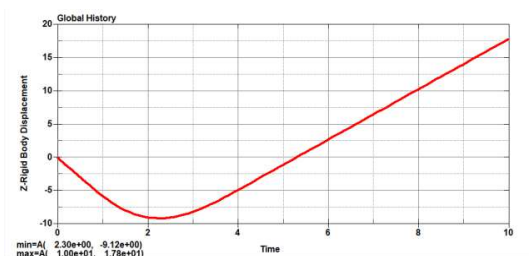


Fig. 15. Predicted impactor displacement versus time, benchmark model.

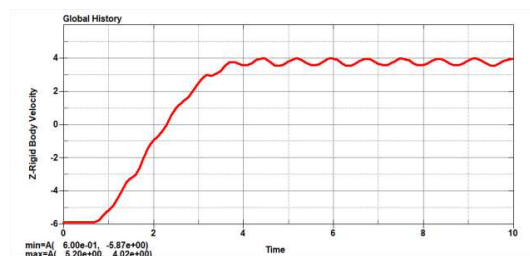


Fig. 16. Predicted impactor velocity versus time, benchmark model.

The predicted maximum impactor displacement is 17.8 mm (Fig. 15), while the impactor maintains its prescribed initial velocity of 6.5 m/s up to the point of contact (Fig. 16), as

expected for a rigid-body initial condition. The maximum von Mises stress, located at the point of contact between the impactor and the laminate, was predicted as 1.069 GPa (Fig. 17), consistent with a highly localised, contact-dominated stress concentration typical of LVI events.

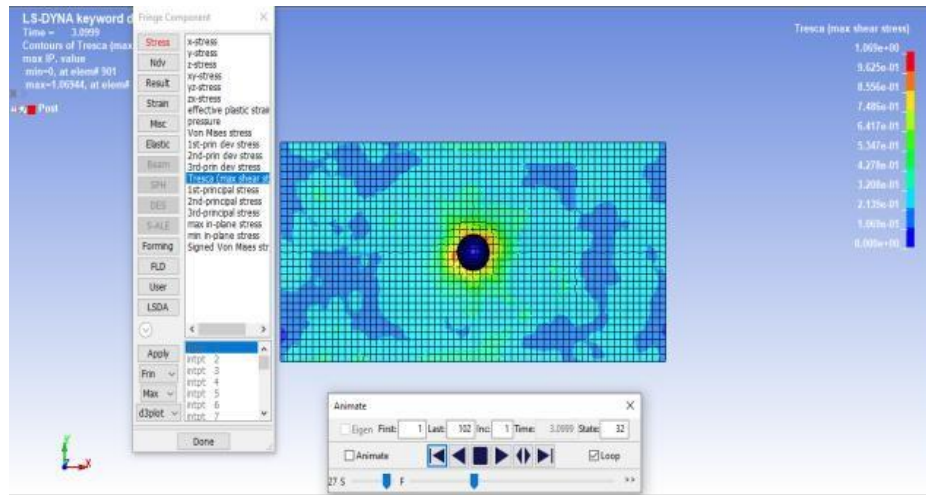


Fig. 17. Von Mises stress distribution at peak contact, benchmark model.

Table 5 Validation of the benchmark FE model against Heimbs et al. [3]

Parameter	Experimental [3]	LS-DYNA (present study)	% Error
Resultant force (N)	9.8	9.8	0
Total energy (J)	38	37	2.63

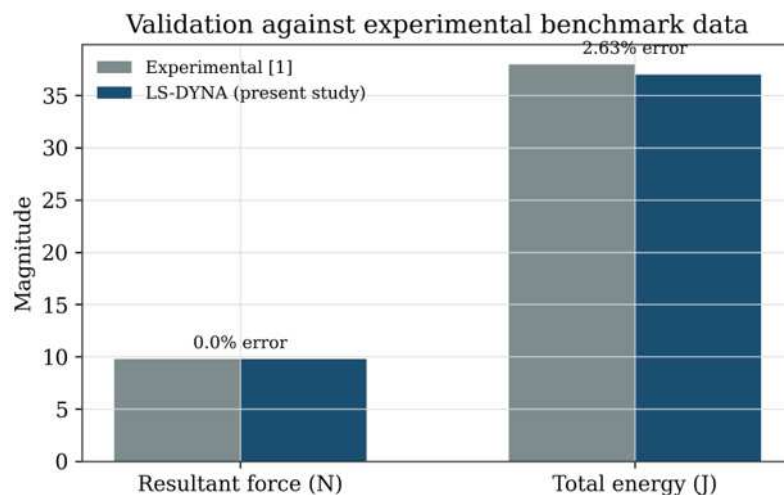


Fig. 18. Comparison of experimental and simulated benchmark response.

The predicted peak contact force matches the experimental value exactly (0% error), and the predicted absorbed energy deviates by only 2.63%, which is well within the scatter typically

reported for explicit composite impact simulations relative to physical test data, and is comparable to the agreement levels reported for related shell-based and continuum-shell benchmark validations [4,12]. This level of agreement confirms that the mesh density, contact formulation, and *MAT_054/055 parameterisation adopted here provide a suitably validated basis for the parametric case studies presented below.

3.2 Effect of Laminate Thickness (Case Studies I and III)

Case Studies I and III examine the effect of increasing laminate thickness from 5 mm to 7 mm under the Chang–Chang and Tsai–Wu criteria, respectively (Table 6, Figs. 19–20).

Table 6 Effect of laminate thickness on impact response

Parameter	Chang–Chang, 5 mm	Chang–Chang, 7 mm	Tsai–Wu, 5 mm	Tsai–Wu, 7 mm
Resultant force (N)	19.6	36.5	19.6	36.5
Plate energy (J)	38.1	39.5	38.1	39.5
Impactor velocity (m/s)	6.40	5.37	4.58	5.37
Impactor displacement (mm)	31.4	43.8	33.1	43.8

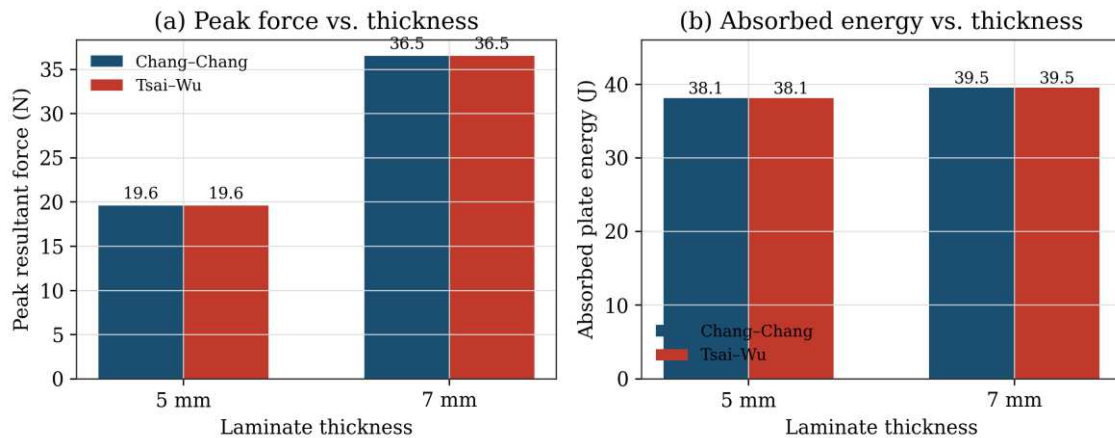


Fig. 19. Peak resultant force and absorbed plate energy as functions of laminate thickness.

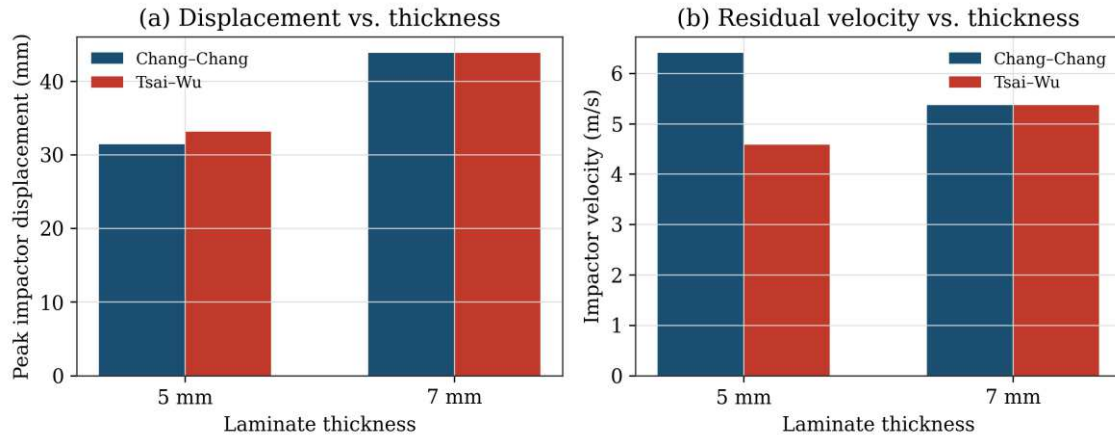


Fig. 20. Peak impactor displacement and residual impactor velocity as functions of laminate thickness.

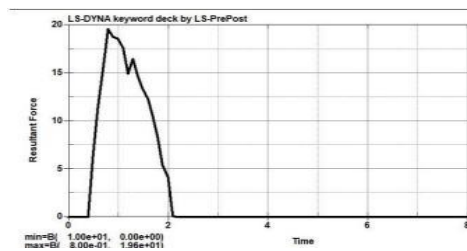


Fig. 21. Simulated reaction force–time trace, Chang–Chang, 5 mm laminate.

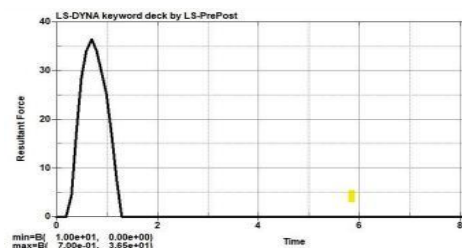


Fig. 22. Simulated reaction force–time trace, Chang–Chang, 7 mm laminate.

Increasing the laminate thickness from 5 mm to 7 mm nearly doubles the peak resultant force (19.6 N to 36.5 N, +86%) and modestly increases the absorbed plate energy (38.1 J to 39.5 J, +4%), consistent with the higher bending and membrane stiffness of the thicker section, which resists penetration more strongly and engages a larger volume of material in resisting the impact. The residual impactor velocity decreases with increasing thickness under both criteria (6.40 to 5.37 m/s for Chang–Chang; 4.58 to 5.37 m/s for Tsai–Wu at the thinner case, converging with the Chang–Chang value at 7 mm), indicating that a larger share of the impactor's kinetic energy is retained by the stiffer plate rather than returned to the rebounding impactor. The peak impactor displacement increases with thickness (31.4 to 43.8 mm), which at first appears to contradict the intuitive expectation that a stiffer plate should deflect less; however, this trend is consistent with the higher peak force and larger absorbed energy –the thicker laminate resists penetration with a steeper force gradient over a longer force–displacement path, producing a larger peak impactor travel before rebound. A structurally analogous coupling between local contact stiffness and global flexural response has been reported for continuum-shell impact models of varying laminate thickness [12], lending independent support to this interpretation. This coupling should nonetheless be interpreted with

appropriate caution and would benefit from a dedicated mesh- and contact-formulation sensitivity study in future work (Section 5).

3.3 Effect of Impact Energy (Case Studies II and IV)

Case Studies II and IV examine the effect of increasing impact energy from 10 J to 20 J, at the benchmark 2.7 mm thickness, under the Chang–Chang and Tsai–Wu criteria, respectively (Table 7, Figs. 23–24).

Table 7 Effect of impact energy on impact response

Parameter	Chang–Chang, 10 J	Chang–Chang, 15 J	Chang–Chang, 20 J	Tsai–Wu, 10 J	Tsai–Wu, 15 J	Tsai–Wu, 20 J
Resultant force (N)	6.41	7.31	7.34	6.42	7.31	7.45
Plate energy (J)	10.1	14.5	19.5	10.1	14.5	19.5
Impactor velocity (m/s)	3.02	3.25	3.54	3.02	3.25	3.47
Impactor displacement (mm)	14.7	15.8	16.0	14.8	15.8	16.7

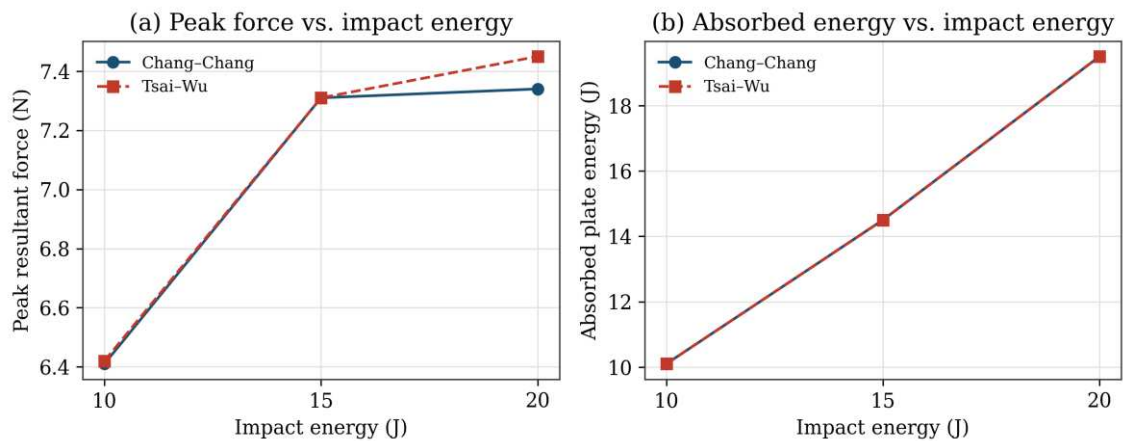


Fig. 23. Peak resultant force and absorbed plate energy as functions of impact energy.

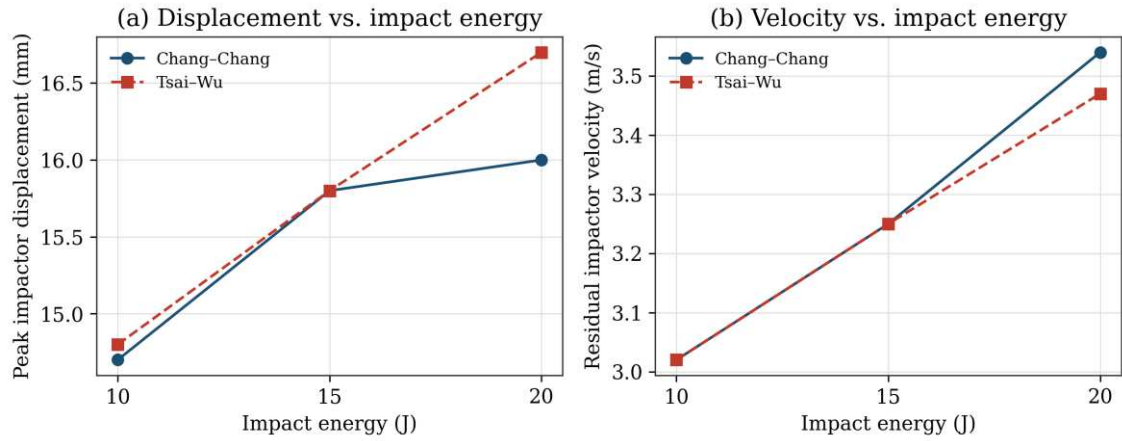


Fig. 24. Peak impactor displacement and residual impactor velocity as functions of impact energy.

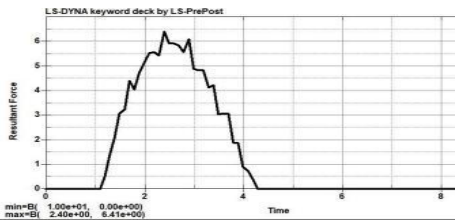


Fig. 25. Simulated reaction force-time trace, Chang-Chang, IE = 10 J.

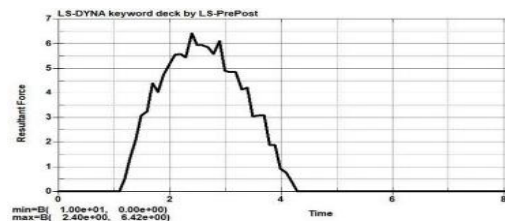


Fig. 26. Simulated reaction force-time trace, Tsai-Wu, IE = 10 J.

Peak resultant force, absorbed plate energy, residual impactor velocity, and peak impactor displacement all increase monotonically with impact energy under both failure criteria, as expected from basic energy and momentum considerations: a higher initial kinetic energy imparts a larger peak contact force, a larger volume of absorbed strain energy, and correspondingly larger residual velocity and displacement once the elastic capacity of the local contact zone is engaged. The rate of increase in resultant force diminishes at higher energy levels (6.41 $\rightarrow$ 7.31 $\rightarrow$ 7.34 N for Chang-Chang between 10 J and 20 J), suggesting the onset of energy dissipation through damage mechanisms - matrix cracking and incipient delamination - that limit further growth in peak contact force even as absorbed energy continues to rise nearly linearly (10.1 $\rightarrow$ 14.5 $\rightarrow$ 19.5 J). This behaviour is consistent with a transition from a predominantly elastic contact response at low impact energy to a progressively damage-dominated response as the impact energy approaches the BVID threshold, a transition that has also been reported in multi-impact and residual-strength studies of CFRP- laminates [9,10].

3.4 Damage Initiation and Stress Distribution

The von Mises stress field obtained for the benchmark case (Fig. 17) shows the expected concentration of stress directly beneath the point of contact, with the peak value (1.069 GPa) locally exceeding the transverse tensile and shear strengths reported in Table 2, consistent with

the onset of matrix-dominated damage at the impact site rather than gross fibre failure. Because damage initiates locally and is confined to a small volume around the contact zone, the global force–time and energy response remains smooth and repeatable across the parametric matrix (Sections 3.2–3.3), even as the underlying ply-level failure criterion is varied -a point examined further in Section 3.6.

3.5 Combined Effect of Thickness and Impact Energy

Considered together, the two parametric axes investigated in this study -laminate thickness and impact energy -exert complementary but distinguishable influences on the impact response. Increasing thickness primarily raises the structural stiffness and contact resistance of the laminate, producing a strongly non-linear increase in peak force relative to a comparatively modest increase in absorbed energy (Section 3.2). Increasing impact energy, by contrast, primarily raises the input kinetic energy available for absorption, producing a strongly linear increase in absorbed energy relative to a comparatively modest and saturating increase in peak force (Section 3.3). From a design perspective, this suggests that laminate thickness is the more effective parameter for limiting peak contact force -and hence local stress concentration and fibre-dominated damage -while control of impact energy at source (e.g., through sacrificial outer plies or discrete impact limiters) is the more effective lever for limiting the total energy absorbed by, and hence the cumulative damage within, the structural laminate.

3.6 Comparative Assessment of the Chang–Chang and Tsai–Wu Criteria

Across all four case studies, the Chang–Chang and Tsai–Wu criteria produced nearly identical predictions of peak resultant force and absorbed plate energy (Tables 6–7), with differences of less than 2% in all but one comparison -impactor velocity at 5 mm thickness, where Chang–Chang predicts 6.40 m/s against 4.58 m/s for Tsai–Wu. This close agreement in the bulk structural response is consistent with the underlying mechanics of the two formulations: because both criteria are post-processing failure checks applied on top of the same underlying orthotropic elastic-damage stiffness model (*MAT_054/055), the global load path and energy balance are governed predominantly by the undamaged elastic stiffness of the laminate rather than by which failure envelope is used to flag ply failure. A comparable insensitivity of the global force–energy response to the choice of ply-level failure criterion has been reported in wider comparative assessments of failure theories for LVI [4], reinforcing the generality of the present finding.

The two criteria are nevertheless expected to diverge more substantially in the predicted spatial extent, shape, and mode (fibre- versus matrix-dominated) of ply-level damage, since the Chang–Chang formulation separates fibre- and matrix-dominated failure into independent mode-specific expressions (Eqs. 1–4), while the Tsai–Wu formulation combines all stress components into a single interactive envelope (Eq. 5) that cannot itself discriminate the governing failure mode. Recent rationalisations of the Tsai–Wu interaction coefficient [7,13] specifically target this limitation and may offer a route to reconciling the mode-discrimination gap between the two formulations without sacrificing the computational economy of the single-envelope approach. A full assessment of this distinction would require systematic comparison of ply-by-ply damage contour plots between the two criteria, which is identified as a priority for future work in Section 5.

4. Conclusions

A validated FE methodology for simulating LVI on CFRP- laminates was developed in LS-DYNA and used to conduct a parametric comparison of laminate thickness and impact energy under the Chang–Chang [1] and Tsai–Wu [2] failure criteria. The principal conclusions are as follows.

- Modelling accuracy: the validated benchmark model achieved 0% error in peak resultant force and 2.63% error in absorbed energy relative to the experimental benchmark of Heimbs et al. [3].
- Effect of thickness: increasing laminate thickness from 5 mm to 7 mm raised peak contact force by 86% and absorbed energy by 4%, identifying thickness as the dominant driver of peak contact force.
- Effect of impact energy: raising impact energy from 10 J to 20 J produced an approximately linear increase in absorbed energy, while peak contact force exhibited clear saturation, consistent with the onset of damage-mediated energy dissipation.
- Failure criteria: the Chang–Chang and Tsai–Wu criteria yielded nearly identical global force and energy responses across all four case studies, though ply-level damage mode and extent are expected to vary between the two formulations.
- Design application: the validated modelling approach provides a computationally economical basis for predicting impact damage resistance and BVID extent in impact-tolerant composite structures for aerospace and automotive applications.

5. Future Work

Several directions extend naturally from the present study. First, the long-term effects of compressive and tensile preload on the LVI behaviour of CFRP plates warrant further investigation, particularly with respect to fatigue life and cumulative damage accumulation under sustained in-service loading [8] -a factor of direct relevance to aerospace and automotive structures subjected to variable loading throughout their service life. Environmental coupling, including the influence of temperature and humidity on the impact performance of preloaded CFRP structures, represents a further extension of practical importance.

Second, systematic ply-by-ply comparison of the damage contours predicted by the Chang–Chang and Tsai–Wu criteria -including damage mode (fibre versus matrix), area, and through-thickness distribution -is recommended to substantiate the hypothesis proposed in Section 3.6 that the two criteria diverge principally at the local damage level rather than in the global structural response. Incorporating a rationalised Tsai–Wu interaction coefficient [7,13] into this comparison may help clarify whether the observed mode-discrimination gap can be narrowed without recourse to the fully mode-separated Chang–Chang formulation.

Third, extension of the present single-impact matrix to repeated and multi-site impact scenarios [5,9] and to residual post-impact strength (compression-after-impact) assessment [10] would connect the predicted BVID footprint more directly to structural certification requirements. Fourth, future work may incorporate more advanced numerical techniques, including cohesive-zone or tiebreak contact formulations for explicit delamination modelling, and multi-scale approaches linking microstructural fibre/matrix interactions to macroscopic impact response. Such refinements would improve the fidelity of damage-evolution predictions and could be complemented by data-driven or machine-learning-assisted calibration of material and damage parameters as additional experimental datasets become available.

Declarations

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Clinical Trial Registration

Not applicable. This study is not a clinical trial and does not involve clinical trial registration.

Consent to Publish

Consent to Publish declaration: Not applicable.

Ethics and Consent to Participate

Ethics approval: Not applicable. This study did not involve human participants, human subjects, or identifiable personal data requiring ethical approval.

Consent to participate: Not applicable. The study did not involve human participants.

Data Availability: The datasets generated during and/or analysed during the current study are available from the corresponding author on reasonable request.

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