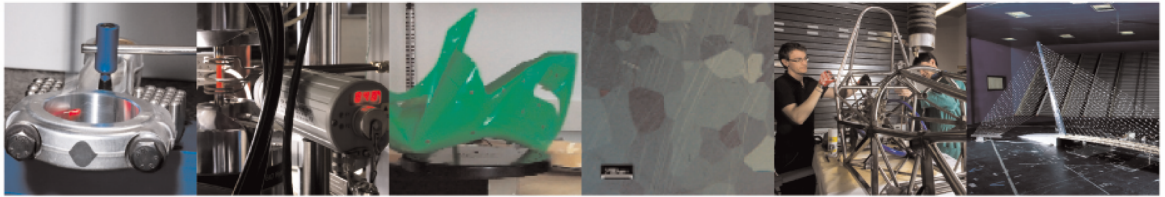




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Numerical study on the dynamic progressive failure due to low-velocity repeated impacts in thin CFRP laminated composite plates.

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Abstract

In this paper, the mechanical response of Carbon Fibre Reinforced Polymer (CFRP) plates subjected to repeated low-velocity impacts is numerically investigated. 3D models of composite plates subjected to repeated low-velocity impacts were developed in Abaqus/Explicit software. The numerical models were validated using experimental data obtained from impact tests on CFRP plates using drop weight tower methodology through the comparison of commonly used parameters (i.e., maximum contact force, maximum impactor displacement, and energy absorption). Damage is intended as several modes for intralaminar failure such as fibre or matrix breakage and interlaminar failure (i.e., delamination). Puck failure criterion with linear strain softening was employed for acknowledging intralaminar failure. Additionally, the bi-linear traction separation cohesive zone model was employed to account for interlaminar damage by defining zero thickness cohesive elements at the interfaces. A sequence of impacts was applied to the composite plate at three different energy levels. It was found that the impact time and the peak force increase in the sequential impacts which indicates a change in the stiffness of the laminate in sequential impacts. The analyses of the results of the damage accumulation and delamination in CFRP plates showed that most of the intra-laminar and interlaminar damage is limited to a few initial impacts in a repeated impact scenario.

Keywords: Composite plates, Low-velocity impact, Repeated impact, Numerical simulation

1. Introduction

There has been a high demand for Carbon Fibre Reinforced Polymer (CFRP) composites in automotive and aerospace industries over the past decades due to their distinguished mechanical properties, particularly their optimal weight-to-strength and weight-to-stiffness ratio. However, composite materials present a criticality due to their vulnerability to out-of-plane impact loading. In fact, during the service life, a component made of composites is subjected to various loading conditions which might generate complex state of stresses promoting several damage mechanisms and coupling among them. The heterogeneous nature of composites further worsens this criticality. The proper understanding of the failure mechanism of composite materials to complex stress of stresses, as it is the case of low velocity repeated impact, is important for implement a reliable design of composite structures. Out-of-plane single impact loading is presently a popular research topic among structural engineers; however, in most realistic cases, the impact event does not consist of a single localised impact case but of a multiple hit situation. Furthermore, a single impact on a CFRP component may not lead to any damage, but the damage accumulation caused by the repetition of impacts may significantly increase the probability of load bearing capability reduction of CFRP and, analogously, the probability of unexpected failure. If these two facts are added to the complex failure process of this type of materials, the necessity to analyse CFRP composites subjected to multiple impact events becomes evident.

Considering the abundance of studies on the response of laminated composites subjected to a single low-velocity impact, very few among them have been conducted on multiple impacts [1–5]. It was proven that damage accumulation can be expected from repeated impacts that individually do not cause any damage in a single hit [6]. Since in real events, the damage accumulation by repeated impacts can cause major reduction in mechanical properties which

has been observed for a variety of materials [4,7–11]. Wyrick and Adams [12] reported that in a sequence of impacts, the first hit causes the major damage and the damage in CFRP composites increases with each impact. Although the response and damage accumulation of composites were mainly investigated experimentally, numerical simulations are needed to better understand the dynamic behaviour, stress-strain response, and damage mechanism in composites subjected to repeated impacts. Another major reason that justifies the need for accurate numerical simulations is that the experimental campaigns are highly time consuming and costly and a purely “trial and test” approach may not be satisfactory for design purposes. Amaro et al. [13] performed single and multiple impact simulations on circular Glass Fibre Reinforced Composite (GFRP) plates considering different low-velocity impact energy levels. Their model was based on the cohesive mixed-mode damage model to account for the delamination between layers; conversely, no intra-laminar failure was considered by the author, simply implementing the elastic behavior of the plies. Their findings showed that a 3 J single impact caused more damage to the plate compared to a multiple case with the same cumulative energy level, i.e., 1 J + 1 J + 1 J and 1 J + 2 J. In experimental and numerical research conducted by Wang et al. [14] the response of braided composites subjected to low-velocity impact was investigated. In their simulations two layers of braided composite were connected by a layer of interface to account for the delamination. Interlaminar behavior was implemented using both element and surface based cohesive zone methods. Instead, intralaminar failure was mimicked using the Hashin failure criterion [15]. Their results showed that both cohesive zone models can be utilized as interface elements in low-velocity impact simulations. Another numerical study of the response of cross-ply composite laminates, in which the Hashin failure criterion was implemented through a user-defined VUMAT material, was performed by Zhou et al. [16]. They observed an increase in the impact peak force in the second impact in all energy levels,

and a gradual increase in the maximum displacement and contact time with the number of impacts.

There is an increasing application of the continuum damage mechanics (CDM) approach for simulation of CFRP composites in the literature [17–19]. Particularly, many publications are based on the implementation of a variety of failure models in different FEM software showing satisfactory results in the prediction of the dynamic and failure response of these composites.

The Hashin criterion is very popular, however it has several drawbacks which are partially compensated for by the Puck model. Indeed, this material model has been demonstrated to be the most accurate model in predicting experimental tests on CFRP [20,21].

The current study addresses the issue by numerically investigating the response of CFRP composite plates subjected to low-velocity repeated impact. The Puck failure criterion, which is a CDM based progressive failure model, and layers of zero thickness cohesive interlaminar elements were used to account for the failure modes of the fibre and matrix as well as delamination. Puck criterion is implemented in Abaqus FEM package using a user-defined material model, i.e., VUMAT. The use of Puck failure criterion and considering the non-linear shear behaviour led to a better knowledge of the response and damage behaviour of CFRP composites subjected to low velocity impact comparing to the similar studies in the literature.

The repeated impacts are simulated employing a sequence of impact hits to the plate through a multi-step concept. Firstly, the plate is hit by the striker and it undergoes a loading-unloading. This is followed by another step in which artificial damping is introduced to the model to mitigate the oscillations and residual elastic vibrations. Finally, the plate is subjected to the next impact load and the sequence is repeated. Using the developed model that takes into account both interlaminar and intralaminar damage, the progressive failure, resulted from repeated low-velocity impacts are investigated to specify the importance and severity of the damage resulted

from repeated loads and can be extended as a reliable approach in many engineering applications.

This paper is organized in four main sections as follows: section 2 discusses the progressive damage model and Puck failure criterion. The numerical simulation procedure such as the geometry, boundary conditions, mesh, etc. are explained in section 3. In section 4 the results of the validation and numerical simulations are reported and discussed. Finally, the conclusions are presented in section 5.

2. Experimental procedure

Drop weight equipment with a hemispherical impactor tip and a total impactor mass of 1.325 kg was used to perform low-velocity impact tests on $150 \times 100 \text{ mm}^2$ fully clamped rectangular specimens at three energy levels of 8 J, 10 J, and 12 J according to ASTM D7136 [22]. The hemispherical impactor diameter was equal to 16 mm, and the impactor was trapped after the impact to prevent subsequent impact to the specimens as shown in Fig. 1 [23].

The 24-layer laminate specimens were manufactured using unidirectional pre-preg laminae CYCOM® 977-2-34%-24K IMS-196-T1 with a stacking sequence of $[45^\circ, 0^\circ, -45^\circ, 90^\circ]_{3s}$. The total thickness of the specimen was 4.8 mm which means a thickness of 0.2 mm for each ply layer.

3. Numerical simulation

3.1 Progressive damage model

The Puck failure criteria has proven to be one of the most accurate material models for the prediction of failure in composites [20,21]. It relies on the distinction between two overall typologies of failure defined as fibre failure and inter-fibre failure (IFF), being the latter the one which shows most particularities. Indeed, depending on the state of stress of each material point, three different IFF modes can be identified. This fact led to the building of the failure locus

presented in the original work of Puck. The key factor of this failure model is the acknowledgement of a critical angle for the failure surface [24]. Furthermore, failure of the composite is caused by the stresses acting on the mentioned fracture plane. Consequently, the failure equation must be expressed in terms of the stress tensor acting on the fracture plane as it can be seen from Table 1. The failure is predicted when the correspondent failure variable ($r_{it,ic}$) is larger or equal to zero.

Table 1. Failure initiation criteria.

Failure typology	Loading direction	Failure equation
Fibre	Tension ($\sigma_{11} \geq 0$)	$r_{ft} = 1 - \left(\frac{\sigma_{11}}{S_{11t}}\right)^2$
	Compression ($\sigma_{11} < 0$)	$r_{fc} = 1 - \left(\frac{\sigma_{11}}{S_{11c}}\right)^2$
Inter-fibre	Tension ($\sigma_{nn} \geq 0$)	$r_{mt} = 1 - \left[\left(\frac{\sigma_{nn}(\theta)}{S_{22t}}\right)^2 + \left(\frac{\tau_{nt}(\theta)}{S_{23}^A}\right)^2 + \left(\frac{\tau_{nl}(\theta)}{S_{12}}\right)^2 \right]$
	Compression ($\sigma_{nn} < 0$)	$r_{mc} = 1 - \left[\left(\frac{\tau_{nt}(\theta)}{S_{23}^A + \mu_{nt}\sigma_{nn}}\right)^2 + \left(\frac{\tau_{nl}(\theta)}{S_{12} + \mu_{nl}\sigma_{nn}}\right)^2 \right]$

* with $S_{23}^A = \frac{S_{22c}}{2} \left(\frac{1 - \sin(\varphi)}{\cos(\varphi)} \right)$, $\varphi = 2\theta - \frac{\pi}{2}$, $\mu_{nt} = \tan(\varphi)$ and $\mu_{nl} = \mu_{nt} \frac{S_{12}}{S_{23}^A}$

In this table, S_{11t} and S_{11c} are the strength of the material in the fibre direction for tension and compression respectively; S_{22t} is the material strength in the matrix direction under tension; S_{12} is the shear strength in direction 12; S_{23}^A is the shear strength in direction 23 but expressed on the fracture (or action) plane since the nt , or perpendicular-perpendicular direction, is the only one affected by the rotation of the stress tensor along axis 1. When a compression state of stress ($\sigma_{nn} < 0$) on the matrix direction is considered, the normal component σ_{nn} impedes fracture and only the shear components are the ones promoting IFF. Therefore, the normal term is not present in the expression corresponding to this stress state. Furthermore, under this stress state the presence of an inclined fracture plane leads to an increase of the ultimate stress due to

friction; this is considered by adding the terms $\mu_{nt}\sigma_{nn}$ and $\mu_{nl}\sigma_{nn}$ to the correspondent equation.

The stresses on the fracture plane are calculated by rotating the stress tensor, expressed by the material direction, around the fibre or direction 1 by an angle θ . In the present work, a constant angle of 53° [25–27] was chosen to reduce the computational time. This angle was determined using a uniaxial compression test. It is known that the angle of the fracture plane might vary with the state of stress considered however, the implementation of a fracture plane angle search algorithm greatly increases the computational time not necessarily leading to a high increase of accuracy. As several simulations should be implemented in this work (i.e., repeated impact), the computational time was a critical factor and so, it was decided to assume the fracture angle plane to be constant. The mechanical properties E_1 , E_2 , ν_{12} , S_{11t} , S_{22t} , S_{12} , c_1 , c_2 and c_3 of the composite material were experimentally measured by carrying out tensile tests and the results are shown in Fig. 2. Fibre and transverse direction stress-strain curves are presented in Fig. 2 (a) and (b), respectively. The properties were determined from tensile tests in accordance with the ASTM standard D3039 [28]. Fig. 2 (c) shows the in-plane shear stress-strain curve that was determined from tensile tests on $[\pm 45^\circ]_{2s}$ test specimens according to the ASTM D3518 [29]. The shear modulus G_{13} was set equal to G_{12} . The remaining parameters were taken from the literature [30,31] reporting on a similar material. The obtained values are presented also in Table 2.

Table 2 Material properties for CFRP composite laminate.

Lamina Properties	
Density	$\rho = 1620 \text{ kg/m}^3$
Young's modulus	$E_1 = 157.5 \text{ GPa}; E_2 = E_3 = 9.9 \text{ GPa}$ $G_{12} = G_{13} = 4.95 \text{ GPa}; G_{23} = 3.21 \text{ GPa};$
Poisson's ratio	$\nu_{12} = \nu_{13} = 0.24; \nu_{23} = 0.35$ [30]
Non-linear shear properties	$c_1 = 10.2 \text{ MPa}; c_2 = -0.5 \text{ MPa}; c_3 = 0.1 \text{ MPa}$
Strength	$S_{11t} = 2550 \text{ MPa}; S_{11c} = 1350 \text{ MPa}$ $S_{22t} = 57.7 \text{ MPa}; S_{22c} = 199.8 \text{ MPa}$ [31]

	$S_{12} = 97 \text{ MPa};$
Fracture energy	$G_{ft} = 133 \text{ N/mm}; G_{fc} = 40 \text{ N/mm}$ $G_{mt} = 0.6 \text{ N/mm}; G_{mc} = 2.1 \text{ N/mm}$ [30]
Interface properties	
Elastic modulus	$E_n = E_s = E_t = 5 \text{ GPa/mm}$ [16]
Strength properties	$N = 33 \text{ MPa}; S = T = 54 \text{ MPa}$ [32,33]
Fracture energy	$G_n^C = 0.6 \text{ N/mm}; G_s^C = 2.1 \text{ N/mm}$ [16]

3.2 Constitutive model

It is widely known that the behaviour of composite materials is highly non-isotropic due to the dominance of each component in different directions. Modelling approaches that consider all the constituents, such as meso-heterogenous approaches, have become important but they are still limited to a small portion or coupon as they require substantial computation efforts. Consequently, macro-homogenous approaches are still the most efficient choice, especially if aimed at modelling large components and or extreme loading conditions. However, constitutive behaviour and damage mechanism have to be accurately reproduced and this poses a challenge in defining models with a trade-off between accuracy and efficiency. In the case of carbon fibre/epoxy composites, the material shows the characteristic linear elastic behaviour until failure in the fibre and pure matrix direction (i.e., 0° and 90° loading directions respectively in a unidirectional lamina). Conversely, in the directions dominated by shear loading (e.g., composites with $\pm 45^\circ$ layup) the behaviour is non-linear. For this reason, the implementation of a non-linear law for the shear stress-strain relationship was considered a key factor in order to fully describe the state of stress inside the composite. Particularly, a third-degree polynomial equation (equation (1)) was employed to represent the stress-strain curves for the τ_{12} and τ_{23} . This method was original proposed in [18,30] to capture the nonlinear shear behaviour and the permanent indentation of a low velocity impact.

$$\tau_{ij} = c_1 \varepsilon_{ij}^3 + \text{sgn}(\varepsilon_{ij}) c_2 \varepsilon_{ij}^2 + c_3 \varepsilon_{ij} \quad (1)$$

Parameters a_3 , a_2 and a_1 are obtained by fitting the exposed polynomial equation to the experimentally obtained shear stress-strain curves.

The practical limitation of the explicit consideration of a micro-crack in a macro-scale composite FE model has led, in the past decades, to the generation of methods which contemplate the effect of the cracks generated during loading in a homogenized fashion. Among the possible approaches, the CDM has been widely chosen to represent the decrement of the load bearing capability in composites due to the presence of micro-cracks. It is based on the degradation of the stiffness matrix as the density of micro-cracks increases and thus, the effective load bearing section of the composite. Mathematically, the degradation is carried out by the definition of several damage parameters, one for each failure mode. These parameters will vary from 1 (i.e., compliance of the failure initiation criteria) to 0 (i.e., complete degradation of the material). Several works [34] proposed a damage matrix whose multiplication by the stiffness matrix gives a damage stiffness matrix. However, here the damage stiffness matrix was defined component-by-component instead of a definition as an inference of the damage stiffness matrix as a linear combination of stiffness matrix and damage matrix. The damage stiffness matrix component definition is based on the study performed by [34] which pointed out the necessity of degrading the Poisson's terms. The damage stiffness matrix is presented in equation (2).

$$dC = \begin{bmatrix} d_f E_1 (1 - d_m v_{23} v_{32}) / \Delta & d_f E_1 (d_m v_{21} - d_m v_{31} v_{23}) / \Delta & d_f E_1 (v_{31} - d_m v_{21} v_{32}) / \Delta & 0 & 0 & 0 \\ & d_m E_2 (1 - d_f v_{13} v_{31}) / \Delta & d_m E_2 (v_{32} - d_f v_{12} v_{31}) / \Delta & 0 & 0 & 0 \\ & & E_3 (1 - d_m d_f v_{12} v_{21}) / \Delta & 0 & 0 & 0 \\ & \text{symmetric} & & * G_{12} & 0 & 0 \\ & & & & G_{13} & 0 \\ & & & & & d_{ms} G_{23} \end{bmatrix} \quad (2)$$

$$\Delta = (1 - d_f d_m v_{12} v_{21} - d_m v_{23} v_{32} - d_f v_{31} v_{13} - 2 d_f d_m v_{21} v_{32} v_{13})$$

Being v_{12} , v_{21} , v_{23} , v_{32} , v_{13} and v_{31} the Poisson's coefficient in each direction, E_1 , E_2 , and E_3 the longitudinal elastic moduli and G_{12} , G_{23} and G_{13} the shear moduli. It should be noted that

no degradation was considered in the out-of-plane direction (i.e., direction 3). Instead, the material behaves as instantaneously failure in this direction. The damage variables for the fibre (d_f), matrix (d_m) and matrix in shear (d_{ms}) are a combination of the tensile and compression damage variables as expressed in equation (3).

$$\begin{aligned}d_f &= 1 - (1 - d_f^t)(1 - d_f^c) \\d_m &= 1 - (1 - smtd_m^t)(1 - smcd_m^c) \\d_{ms} &= 1 - (1 - d_m^t)(1 - d_m^c)\end{aligned}\tag{3}$$

Where smt and smc are the shear fraction in matrix tension and matrix compression directions which are equal to 0.9 and 0.5; respectively [35,36]. d_f^t , d_f^c , d_m^t and d_m^c are the damage variables in fibre tension and compression and matrix tension and compression respectively. The degradation phase of the stiffness variables is usually referred to as strain softening and the expression describing how the material is degrading as strain softening law. For the present article, the authors considered a strain-based linear decay of the damage variables as stated in equation (4).

$$\begin{aligned}d_f^{t,c} &= \frac{\varepsilon_{f,u}}{\varepsilon_{f,u} - \varepsilon_{f,fi}} \left(1 - \frac{\varepsilon_{f,fi}}{\varepsilon_f} \right) \\d_m^{t,c} &= \frac{\varepsilon_{eq,u}}{\varepsilon_{eq,u} - \varepsilon_{eq,fi}} \left(1 - \frac{\varepsilon_{eq,fi}}{\varepsilon_{eq}} \right)\end{aligned}\tag{4}$$

Super-indexes t and c represent the cases of tension and compression. Sub-index fi stands for failure initiation that is the strain level at which the failure initiation criteria were met. For the fibre direction case, it is computed as the failure limit in each direction (i.e., XT, XC) divided by the counterpart elastic modulus. Regarding again the matrix damage parameter in equation (4), it can be seen that the concept of equivalent strain is used, the norm of the stress/strain components acting on the fracture plane as expressed in equation (5) is intended as equivalent strain.

$$\varepsilon_{eq,i} = \sqrt{\langle \varepsilon_{n,i} \rangle^2 + \gamma_{nl}^2 + \gamma_{nt}^2} \quad (5)$$

For the matrix dominated direction, the $\varepsilon_{eq,fi}$ is the computed *in-situ* when the Puck criteria is met. The sub-index u in equation (4) stands for ultimate which is the strain level at which the material reaches complete degradation. It is computed using the concept of failure energies for each one of the direction or analogously for each one of the failure mechanisms. They are therefore defined as:

$$\varepsilon_{i,u} = \frac{2G_{i,c}}{\sigma_{i,u}l} \quad (6)$$

Being $G_{i,c}$ the fracture toughness and l the characteristic length of each element. In the literature [37,38] the dependency of the fracture toughness with the element size is clearly stated. This dependency was solved by utilizing the crack-band model presented in [39].

3.3 Interlaminar damage model at the interface

In this study, two different cohesive zone methods, namely cohesive contact and zero thickness cohesive element, were employed to simulate the delamination at the interface of the plies. In both approaches, a bilinear traction-separation curve together with a quadratic stress failure criterion and the mixed-mode fracture energy laws of Benzeggaghand Kenane (B-K) [40] was used to predict the initiation and propagation of the delamination (Fig 3). The quadratic stress failure criterion can be presented as:

$$\frac{\langle t_n \rangle^2}{N^2} + \frac{t_s^2}{S^2} + \frac{t_t^2}{S^2} = 1 \quad (7)$$

In which $\langle \rangle$ is Macaulay bracket to only allow for damage when tensile normal traction acts on the interface, t_n is normal traction, t_s and t_t are shear tractions in both in-plane directions,

and N and S are normal strength and shear strength at the interface, respectively. While, after
the initiation of the delamination, the corresponding damage evolution is describes based on the
(B-K) law which indicates the cohesive stiffness degradation, as expressed in equation (8).

$$G_n^C + (G_s^C - G_n^C) \left\{ \frac{G_s}{G_T} \right\}^\eta = G^C \quad (8)$$

Where $G_s = G_n + G_t$ and $G_T = G_n + G_s$ represent the amount of dissipated energies. G_n^C, G_s^C ,
and η are the critical normal fracture energy, critical shear fracture energy, and (B-K) material
constant, respectively [41]. The material properties for the interface models are summarized in
Table 2.

3.4 Geometry and mesh of FEM model

A full-scale 3D numerical model of CFRP laminate was developed in Abaqus/Explicit to
further investigate the response and failure behaviour of specimens subjected to repeated
impacts. Fig. 4 shows the Finite element model which consists of 24 layers of lamina,
hemispherical impactor, and a pair of fixed steel clamps to fully replicate the boundary
conditions and contacts that interact in a real experiment. The composite plate was modelled
using 8-node linear brick elements (C3D8R) with reduced integration and enhanced hourglass
control. The element size of 1mm×1mm was chosen for the contact area between the impactor
and the plate to accurately measure the response and failure of the composite plate; however,
outside the 50mm×50mm fine mesh area, the mesh was coarser to reduce the computational
time of the simulations. A mesh sensitivity analysis was performed by considering three
different element sizes of 1.5mm×1.5mm, 1mm×1mm, and 0.75mm×0.75mm to define the
proper element size without affecting the accuracy of the results. Details are reported in section
4.2.

To simulate the interface delamination failure, two different approaches were employed and
compared for a single impact. Cohesive contact interaction or cohesive elements have been

shown to be effective [42] in the simulation of delamination in laminated composites in the literature for a single impact; however, as it will be discussed in the results section, only the zero thickness elements are suitable for simulation of interface damage in a repeated impact event. Therefore, zero thickness cohesive elements (COH3D8) elements were used for the cohesive layers of elements at each interface in the repeated impact models. In addition to the cohesive behaviour at the interface, a general contact domain was applied to the model which is based of penalty algorithm with a friction coefficient of 0.3 for the contact between the composite and the impactor [43], and a friction coefficient of 0.9 for the contact between the clamps and the plate [44–46] to reduce the contact slip at the clamp boundary condition.

3.5 Repeated impact modelling

In this study, a multi-step approach was utilized to simulate the repeated low-velocity impact events which are generally based on the usage of an initial predefined field option in Abaqus/Explicit to transfer the material state of an impact simulation for each element in the laminate to the sequential impact as an initial condition. The material state that can be imported to another impact event using this approach includes the displacements, stress-strain state, and solution-dependent state variables (SDV) which are used to store the material data on integration points of elements in the definition of material by VUMAT. Most important part of the SDVs are the variables that define to record the progression of damage corresponding to different failure modes of Table 1 which are essential for simulation of composite materials subjected to repeated impacts.

Therefore, by importing the SDVs, the propagation of different damage indicators in the elements of each ply is transferred to the next low-velocity impact event and the damaged laminate is loaded in sequential impacts. At the beginning of each repeated impact event, the composite laminate is hit by the hemispherical impactor at the centre, following the complete detachment of the impactor at the end of the first impact the composite laminate is imported to

another step, in this step the laminate is put to rest for mitigation of unwanted elastic oscillations that resulted from the first hit. Then, the laminate is imported to the second hit event in which it is impacted with the same energy level as the first impact. It should be noted that in the successive impact event, the laminate, the impactor, and the clamps are simulated again in a separate model, and like the previous impact, the impactor moves toward the laminate with a velocity initial condition. The only difference is that the initial predefined field option is exploited in the successive impacts in order to import the material state of the previous impact to the successive impact model. This process is continued for a total of five impacts at each energy level. The rest step has previously been shown to be important in the simulation of plates subjected to dynamic loads [47]; however, in this study, the results of the rested plate were compared with the unrested plate to investigate the necessity of the rest step in simulations of repeated low-velocity response of CFRP laminates. The total time of each loading and rest step in the numerical simulations is equal to 2.5 and 5 ms, respectively.

4 Results and discussion

In this section, the validity of the FE model is established by comparing the results of a single low-velocity impact test with the numerical simulation results in terms of impact force, maximum displacement, absorbed energy, and total delaminated area. Then the results of repeated impact simulations are presented. As a prerequisite, the Puck failure criterion and non-linear orthotropic constitutive material model were implemented in Abaqus/Explicit using a VUMAT subroutine. To gain insight into the repeated low-velocity response and failure behaviour of CFRP composite laminates, the simulation results of plates which were subjected to a sequence of five repeated impacts are presented and discussed. Furthermore, the effect of two different approaches, namely, cohesive zero thickness elements and cohesive contact, in the simulation of cohesive behaviour and delamination of CFRP composites are illustrated.

4.1 Validation of the FE model

To demonstrate the reliability of the established FE models, the general impact response and delaminated area of single impact simulations at three different energy levels of 8 J, 10 J, and 12 J were compared with the corresponding low-velocity impact experimental results [48]. Fig. 5 shows a comparison between the numerically predicted force-time history and force-displacement curve of 10 J impact using two different cohesive simulation approaches with the experimental data. Fig. 5 (a) and (b), show that both simulation methods are capable of reasonably predicting the key aspects of the experimental curve and good agreement is observed between the initial slope of the curves, peak force, impact time duration, and maximum indentation displacement comparing to the experimentally observed response. A smoother behaviour was observed from the numerical results of the model with cohesive elements comparing with the experimental data which is characterized with a steeper force drop. This difference may be related to presence of damages that are related to material uncertainties or manufacturing faults that are not predicted by the macro homogeneous mechanical FEM model. However, the high-frequency oscillations of the observed in the force-time numerical curve were not present in the experimental test. The occurrence of these high-frequency oscillations is probably caused by flexural vibration of the specimen. These vibrations are normally avoided because of the intrinsic damping properties of the polymer [49] and were not considered in the present analysis. Moreover, the model with zero thickness cohesive elements shows a smother response compared to the model with cohesive contact. This difference is related to the introduction of damage stabilization for cohesive elements.

Fig. 5 (c) presents a comparison of the numerical and experimental energy time histories for the impact energy of 10 J. After the contact of the impactor, a portion of the initial kinetic energy of the impactor is absorbed by the CFRP plate as elastic deformation, and the rest of the initial kinetic energy is dissipated in form of interlaminar damage, intralaminar damage and friction energy until the velocity of impactor reaches zero that means all of the 10 J kinetic energy is

transferred to the plate. After this, rebounding starts and the elastic energy which was stored in the plate is transferred to the impactor, and finally, the CFRP laminate reaches a final state in which its absorbed energy is a constant value, corresponding this to the impactor complete detachment. According to Fig. 5 (c) both numerical approaches could predict the transformation of the initial impact energy and rebounding of the plate. It is shown that the final absorbed energy of the model with zero thickness cohesive elements and of the model with cohesive contact are equal to 3.66 J, and 3.27 J, respectively. This shows an approximate error of -13.1% for the zero-thickness element model and -22.3% for the cohesive contact model when compared to the experimental absorbed energy of 4.12 J.

As mentioned before both approaches have been extensively exploited in the literature to predict the delamination of laminated composites. Developing the model with zero thickness cohesive element requires the definition of a layer of elements with an extremely low characteristic length that is not computationally effective. However, since the repeated impact behaviour of plates is investigated in this paper, and as mentioned in section 2, the deformed shape and residual state of the composite plate is imported to sequential low-velocity impacts calculations using the initial predefined field option in Abaqus/Explicit, the cohesive contact approach cannot be employed. This is due to the definition of the initial predefined field option in Abaqus/Explicit which only allows the import of element-based outputs to the sequential simulations. Therefore, all the simulations of repeated low-velocity impact were carried out by using the zero-thickness cohesive element approach, and hereafter only the results of simulations with zero thickness cohesive elements are discussed.

To assess the validity of the numerical simulation at other energy levels, the force-time histories, force-displacement curves, and energy-time histories of 8 J and 12 J impacts are shown in Fig. 6. As clearly visible in Fig. 6, good agreement between the peak force, maximum displacement and absorbed energy of the simulation result and experimental data was obtained. The values

of maximum penetration displacement and absorbed energy at different low-velocity impact levels are reported in Table 3. The measured values of absorbed energy error were equal to -23.6%, -13.1%, and -5.7% for low-velocity impacts at 8 J, 10 J, and 12 J impact energy levels respectively. This more precise prediction of absorbed energy at higher energy levels is attributed to the better prediction of delamination at higher energy levels in numerical models as discussed below. Moreover, a comparison between the experimental and numerical results of maximum displacement and absorbed energy are listed in Table 3, the maximum penetration displacement of the impactor is predicted within 4% error that shows better accuracy in the prediction of the deformation of the composite laminate compared to the energy abortion.

Table 3. Comparison of experimental and numerical results of maximum displacement and absorbed energy.

Impact energy (J)	Maximum displacement (mm)			Absorbed energy		
	Experimental	Numerical	Error [%]	Experimental	Numerical	Error [%]
8	2.17	2.21	1.8	3.39	2.59	-23.6
10	2.57	2.47	-3.9	4.21	3.66	-13.1
12	2.81	2.84	1.1	4.87	4.59	-5.7

Fig. 7 shows the comparison between the experimental and numerical delaminated areas at different impact energies of 8 J, 10 J, and 12 J. The projected delaminated region in the experimental test was measured by the c-scan ultrasonic technique. In the numerically predicted delamination, peanut-shaped damage patterns at different interfaces through the laminate are visible which demonstrate propagation of delamination at four different directions of 0° , $+45^\circ$, -45° and 90° corresponding to the directions of fibre in each ply. The experimental results of the total delaminated area are compared with the numerically predicted delamination in Fig. 7 (d). In this figure, the solid line is the zero-error line which illustrates the exact prediction of the numerical results to the experimental data. The prediction errors are equal to 6.4, 6.1, and 0.6 % for low-velocity impacts at 8 J, 10 J, and 12 J energy levels, respectively. The better

prediction of delamination at higher energy levels explains why more precise predictions of absorbed energies are observed at higher impact energy levels. In addition to the size of the delaminated area, the numerical predictions also accurately mimic the circular shape of the damage at all energy levels. Therefore, from an accuracy point of view, zero thickness cohesive elements can be utilized to simulate the low-velocity impact of CFRP laminates.

In order to validate the capability of the proposed numerical method in predicting of repeated impact response and damage propagation of CFRP laminates the repeated low velocity impact of T300/YH69 unidirectional prepreg composite laminates has been simulated according to the experiments of Ref. [50]. The laminates with quasi-isotropic layup of $[45^\circ_2, 0^\circ_2, -45^\circ_2, 90^\circ_2]_{2s}$ and size of $80 \times 80 \text{ mm}^2$ were subjected to multiple low velocity impacts with a hemispherical impactor with the diameter of 16 mm and energy of 15 J at the center. The total thickness of the laminate was 3.84 mm. The mechanical properties and the interface properties of the composite laminate made of T300/YH69 are presented in Table 4 [51,52], while the nonlinear shear constants of equation (1) are assumed to the values of Table 2 due to the lack experimental data for T300/YH69 composite laminate.

The comparison between the experimental and numerical force displacement curves of the first and the fifth impacts is shown in Fig. 8. It is clear from this figure that the present numerical modelling approach is capable of predicting the mechanical response of the laminates in the first impact as well as the fifth one. In addition, as it is discussed in section 4.3, the increase of the peak impact force which was observed in the experimental results is well predicted by the numerical model. The comparison of experimental and numerical characteristics of repeated impact response including the absorbed energy, peak impact force, maximum central displacement, and impact time duration are shown in Fig. 9-a to d, respectively. The present numerical modelling approach is capable of predicting the absorbed energy with a deviation of less than 1 J for all the impact events, while slightly over predicting the peak impact force for

the repeated impact events. Good agreement has been observed between the predicted maximum central displacement and time duration of the repeated impacts with the experimental results of [50]. In addition, the comparison of numerical and experimental delamination projected area for the first, third, and fifth impacts is shown in Fig. 10, showing that the present numerical modelling approach can predict the propagation of damage caused by the repetition of the impact events.

Table 4 Material properties for T300/YH69 composite laminate [51,52].

Lamina Properties	
Density	$\rho = 1620 \text{ kg/m}^3$
Young's modulus	$E_1 = 130 \text{ GPa}; E_2 = E_3 = 7.06 \text{ GPa}$ $G_{12} = G_{13} = 3.08 \text{ GPa}; G_{23} = 3.6 \text{ GPa};$
Poisson's ratio	$\nu_{12} = \nu_{13} = 0.32; \nu_{23} = 0.5$
Strength	$S_{11t} = 1760 \text{ MPa}; S_{11c} = 1100 \text{ MPa}$ $S_{22t} = 51 \text{ MPa}; S_{22c} = 167 \text{ MPa}$ $S_{12} = 70 \text{ MPa};$
Fracture energy	$G_{ft} = 56 \text{ N/mm}; G_{fc} = 10 \text{ N/mm}$ $G_{mt} = 0.25 \text{ N/mm}; G_{mc} = 0.75 \text{ N/mm}$
Interface properties	
Elastic modulus	$E_n = E_s = E_t = 5 \text{ GPa/mm}$
Strength properties	$N = 60 \text{ MPa}; S = T = 80 \text{ MPa}$
Fracture energy	$G_n^C = 0.35 \text{ N/mm}; G_s^C = 1.45 \text{ N/mm}$

4.2 Mesh sensitivity

Fig. 11 shows the impact force-time histories at 10 J impact energy for three different element sizes of 1.5mm×1.5mm (coarse mesh), 1mm×1mm (intermediate mesh), and 0.75mm×0.75mm (fine mesh) at the laminate-impactor contact area. The peak impact force is overestimated by the coarse mesh while the discrepancies between the intermediate mesh and fine mesh peak force result are less than 1%, and the experimental peak force is accurately predicted by both the fine mesh and intermediate mesh. To consider the accuracy and computational efficiency, the intermediate mesh size is chosen for the numerical simulations. A comparison between the predicted areas of tensile matrix damage for a typical ply with 45° orientation is presented in

Fig. 12 for three different contact area element sizes. It is clear from the figure that a similar tensile matrix damage shape and area is predicted by different element sizes indicating that the element size has a slight effect on the damage prediction of the numerical model.

4.3 Repeated impact response

4.3.1 Dual impact response

The numerically obtained force and displacement response of the first and second impacts at three impact energy levels of 8 J, 10 J, and 12 J are presented in Fig. 13. Both impact events were carried out at the centre of the composite laminate (thus exactly in the same place) while the geometry of the plate was changed due to the permanent deflection and residual stresses caused by the first impact. Consequently, the effect of the residual state of the composite laminate is observed in force-time and force-displacement responses of the laminate. The slope of the ascending force-displacement curve which represents the impact bending stiffness, reveals significant stiffness loss at the beginning of the second impact event at all impact energy levels; however, higher stiffness was observed in the following phase of the impact in damaged plates, as discussed in section 4.4. Fitting of a straight line was used in [53,54] in order to estimate the bending stiffness of the laminates subjected to low velocity impact. While a straight line can estimate the stiffness during the first impact of Fig. 13, for the second impact a straight line does not lead to clear estimations of impact bending stiffness. Therefore, the impact bending stiffness of repeated impacts has been discussed in two different time ranges, the initial impact bending stiffness which is the slope of the force displacement at the beginning of the impact, and the ultimate bending stiffness of the force-displacement curve which is the slope at the vicinity of the peak impact force. The comparison between these stiffness values has been performed in Fig. 14.

In addition, Fig. 13 shows an increase in the peak force in the second impact at all impact energy levels which is consistent with previously presented experimental and numerical results in

445 [16,55]. Considering the presence of progressive interlaminar and intralaminar damage at the onset of the second impact, a reduction of the peak force is expected during the second impact due to the degradation of the mechanical properties of the plate in the zone close to the impact point. Yet this unanticipated increase in the peak force can be attributed to the change in the damage response of the laminate which is discussed in detail below. A slight increase in impact
450 time duration was observed at higher impact energy levels at the first impact; while, due to the progress of damage in the second impact (i.e., reduction of stiffness) which resulted in more maximum deflection comparing to the first impact, the impactor needs more time to rebound, therefore a higher impact time duration was observed in sequential impacts.

Comparing the force-time histories of the plate from the first and second impacts, more
455 oscillations and steep changes in the slope are visible in the first impact response of the laminates. These oscillations and changes are due to the dominance of the initiation and propagation of interlaminar and intralaminar damage within the laminate at the first impact event which influences the wave reflection.

4.3.2 Several impacts response

460 In Fig. 14 the energy-time histories of the first five impacts at three impact energy levels are shown. At each impact event, the total kinetic impact energy is completely transferred to the laminate. Part of this impact energy is converted into elastic energy (which is retransmitted to the impactor) and part is absorbed by the specimens, i.e., dissipated during the impact event through damage, permanent deformation, and friction. The absorbed energy ratio is defined as
465 the ratio of the absorbed energy to the impact energy and can be employed to investigate the energy absorption capacity of CFRP laminate which is subjected to repeated impacts. The same trend was observed at all energy levels and the absorbed energy ratio decreased with an increase in the number of impacts. A more considerable decrease was observed at the second impact and after the second impact a steadily absorbed energy ratio was reached. This decrease can be

470 attributed to the damage progress in CFRP laminate subjected to repeated impact. It was
observed that in a repeated impact event, the first hit caused the major damage in CFRP. This
damage is characterised mainly by delamination and matrix failure around the impact point. In
subsequent impacts the failed part was less active in absorbing energy; therefore, the relative
475 damage of each impact (delamination and matrix failure) was less severe compared to the first
impact which is consistent with the experimental observation made in [12].

Also, a time delay in which the total kinetic energy of the plate is transferred into the composite
laminate was visible with the increase of the number of impacts. This increase in the
composite/impactor contact time or delay is due to the higher deflection in the damaged
laminate in sequential impacts. The absorbed energies and maximum displacements of five
480 sequential impacts at three energy levels are summarised in Fig. 15 (a) and Fig. 15 (b),
respectively. The tendency towards a constant absorbed energy ratio is demonstrated in Fig. 15
(a). For instance, in the case of a 12 J impact energy, the absorbed energy ratio decreased from
0.383 for the first impact to 0.243 for the second impact while the corresponding values are
equal to 0.181 and 0.19 for fourth and fifth impacts, respectively. Accordingly, a similar trend
485 was observed for the maximum displacement of the impactor, a significant increase of the
maximum displacement occurred at the second impact while the change in the maximum
displacement in the sequential impacts was roughly equal. Moreover, the transient plate
deformation for five impact events of 10 J is presented in Fig. 16 which shows a larger
deformation plate centre indicating a localised damage.

490 **4.4 Effect of repeated impact on interlaminar and intralaminar damage**

Fig. 17 shows the comparison of the predicted delamination damage at selected interfaces
through the thickness of the CFRP laminate after the first and second impact events at 12 J
energy level. The red areas in the contours represent the initiation of delamination and the
removed elements (in grey) exhibit the complete delamination. It was observed that the

495 delamination initiates in the centre at most interfaces through the laminate thickness; while, at each interface, the orientation of the fibres in the ply below is the determining factor for the direction of the delamination propagation [56,57].

Similarly, at all the selected interfaces of Fig. 17 the delamination propagated according to the fibre orientation of the ply below. For instance, the delamination crack at interface 1 (+45/0) and 11 (-45/90) showed propagation at 0° and 90° , respectively. Moreover, it was observed that although the orientation of the lower ply determined the direction of delamination crack propagation, the top layer fibre orientation influenced the shape of the damaged peanut area, as shown at interface 7 (-45/90) and interface 20 (+45/90) in Fig. 17. By comparing the size of the delaminated area after the first impact and second impact, it is clear that the damage caused by the first impact is more severe and, hence, the major damage in the laminate is caused by the first impact, while minor delamination crack propagation is observed after the second impact in the laminate. This is the reason for the higher impact energy absorption at the first rather than the second impact at the same energy level. Also, the direction of the delamination crack propagation does not change in the sequential impact and is consistent with the results from a single hit.

To further investigate the effect of the repetition of impacts on the delamination area, the results for five sequential impacts at the impact energy of 8 J are compared in Fig. 18 for selected interfaces through the thickness of the CFRP laminate. For the impact events after the second impact, the size of the delaminated area is roughly equal and the rate propagation of delamination is very low; however, an increase in the number of removed elements at some interfaces is observed in the numerical results. The results of the total delaminated area at three impact energy levels of 8 J, 10 J, and 12 J are summarized in Fig. 19. The delamination area reaches a steady state after two impacts at 8 J while the propagation of delamination is not limited to the first two impacts at higher energy levels. By comparing the delaminated area

520 resulted from the impact scenario with the same total accumulative impactor energies, it is clear that the impact events with the fewer number of impacts can cause more damage to the laminate. For example, in the event of two 12 J impact events (12 J+ 12 J) will result in the total delaminated area of 1084 mm² while in case of three 8 J impact events (8 J + 8 J + 8 J) the total delaminated area is equal to 563 mm².

525 In Fig. 20 the force-time history is compared in a double axis graph with the accumulative delamination-time history of the first two impacts at the 10 J impact energy level. As mentioned before, the unexpected increase in the peak force at the second impact is consistent with the experimental findings of [4,55] and the numerical results in [16]. Hosur et al. [4] suggested that due to the degradation of the matrix in the first loading, the peak force is increased due to the
530 more direct contact between the impactor with stiffer fibre during the second impact event.

In addition to the presence of fibres rather than matrix (i.e., stiffer contact) at the contact zone in sequential impacts, the increase in the peak force in the second impact can also be attributed to the changes in the general response and damage propagation during the second impact event. By comparing the results of force-displacement and damage-time histories during the first and
535 second impact events, it is clear that the damage propagation starts at earlier stages of the first impact (around 0.2 ms after the impactor hits the laminate) while there is no evident damage propagation starting point during the second impact event, see Fig. 13. Also, higher stiffness of the intact composite plate at the beginning of the first impact is observed compared to the stiffness on the composite plate at the beginning of the second impact that can be attributed to
540 interlaminar failure and lack of constraint between layers resulting in higher deformations. However damaged plates exhibit a higher stiffness in the following phase of the impact, see Fig. 13.

Moreover, while damage starts to propagate earlier during the first impact the bending stiffness of the starts to decrease, and the kinetic energy of the plate is absorbed by the propagation of

the damage. In contrast, during the second impact at the early stages, there is limited damage propagation in the matrix (interlaminar) and most of the load is supported by the element of the ply (intralaminar) that are mostly composed by fibers. Therefore, the fibres in the damaged region seem to carry the load. However, the lack of interlaminar reaction to the load provoke an increase in the deformation of the plate. With the lack of damage propagation during the second impact, the kinetic energy of the impactor must be transferred to the plate through deformation of the load-carrying elements beneath the impactor itself. Thus, the delay in damage propagation and the higher maximum deformation of the plate in sequential impacts leads to higher peak forces, while the fibres remain intact and continue to carry the load in the laminate.

The comparison of the predicted matrix damage (intralaminar) at selected plies for the first and fifth impact for impact energy of 10 J is presented in Fig. 21. In this figure, the blue-colored areas represent the undamaged area while the red-colored areas stand for the tensile matrix failure prediction in the ply. The matrix damage propagates along the fibre direction in all plies and the damaged area is considerably smaller in the plies near the impact size while bigger tensile matrix damage was observed in the plies that are close to the back side of the laminate. The damage shape is the same for all impact energies while only the size of the damaged area is higher with an increase in the low-velocity impact energy as shown in Fig. 22. Also, the matrix damage propagation direction does not change in sequential impacts while the damaged area is increased in sequential impacts, as expected.

4.5 Effect of rest step

To assess the effect of the rest step on the response of the CFRP laminates subjected to low-velocity impact, a comparison between the typical force-time response of a rested plate and plate without a rest step is presented in Fig. 23. In the model without rest, the laminate is subjected to the sequential impact immediately at the end of the first impact event as shown in

Fig. 23 (a). The total time of the two impact events is equal to 5 ms. However, in the model with the rest step the laminate is imported to an intermediary rest step, resulting in a total simulation time of 10 ms for two impact events. This means that the computational time highly increases with the introduction of the rest step in the simulation process. The results of the second impact force-time history are plotted in the local step time for two different models in Fig. 24. As shown in this figure the two models predict an almost identical response of the laminates indicating that the residual oscillations in the laminate after the first impact event are low and can be neglected in the prediction of the response of the laminate in sequential impacts. It is worth mentioning that the conclusion about the rest step cannot be applied to any circumstances or laminate configuration. As it is discussed in [45], a good indicator of the necessity of rest step is the displacement time curve of the specimen at the point of impact. If there are oscillations on the time response, a rest step would be inevitable. However if there is no unwanted residual vibrations in the response of the laminate, the rest step can be neglected.

5 Conclusion

The mechanical response of CFRP plates subjected to repeated low-velocity impacts was numerically investigated. Composite plates were subjected to a sequence of five repeated impacts at three energy levels of 8 J, 10 J, and 12 J, and the numerical models were validated using experimental data taken from single impact tests on CFRP plates using a drop weight tower. The numerical results showed good agreement with the experimental data in terms of energy absorption capacity, peak displacement and force, and delamination. Cohesive contact and zero thickness cohesive elements were employed to simulate the cohesive behaviour at the interface and zero thickness elements were chosen since they are applicable for repeated impact simulations. Some of the main conclusions withdrawn from this work are:

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- The repeated impact response of the plate showed that the impact time and peak force increased in sequential impacts which indicates a local change in the stiffness of the laminate.
 - The decrease in energy absorption capacity for the second impact, is more considerable and after the second impact, a steadily absorbed energy ratio was reached.
 - 600 • The maximum contact force increased with the number of impacts. These was caused by the delay in the damage propagation and the higher maximum deformation of the plate due to the presence of, mostly, intact fibres.
 - The peanut shape delamination was observed in repeated impact events with the same orientation and only the size of the delaminated area increased with the number of impacts. The largest damage was created during the first impact and the shape and
605 orientation of the delamination did not change with subsequential impacts.

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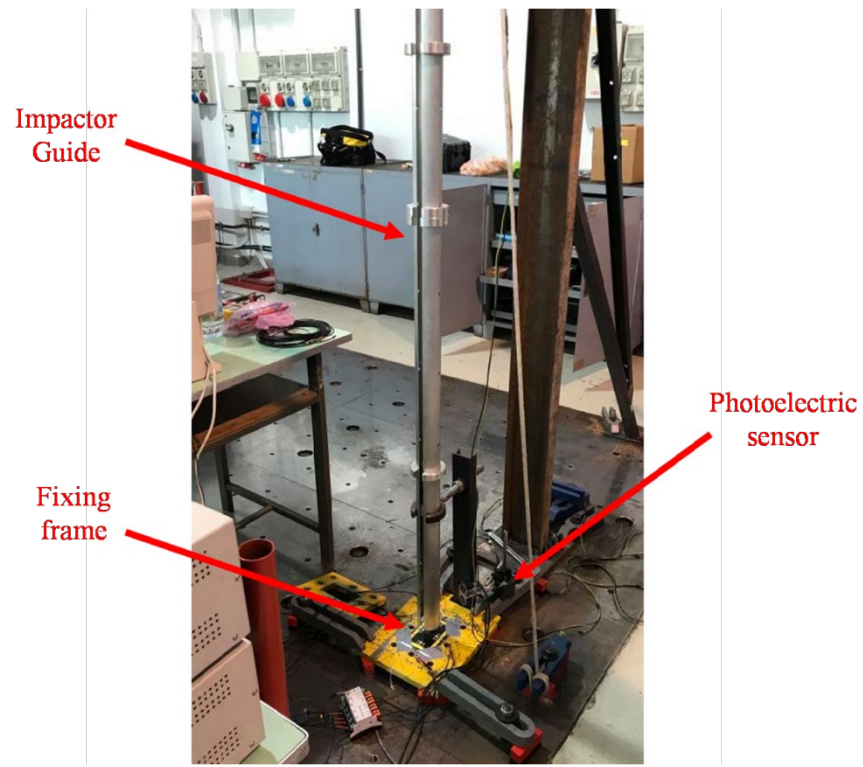


Fig. 1. Drop weight impact test setup.

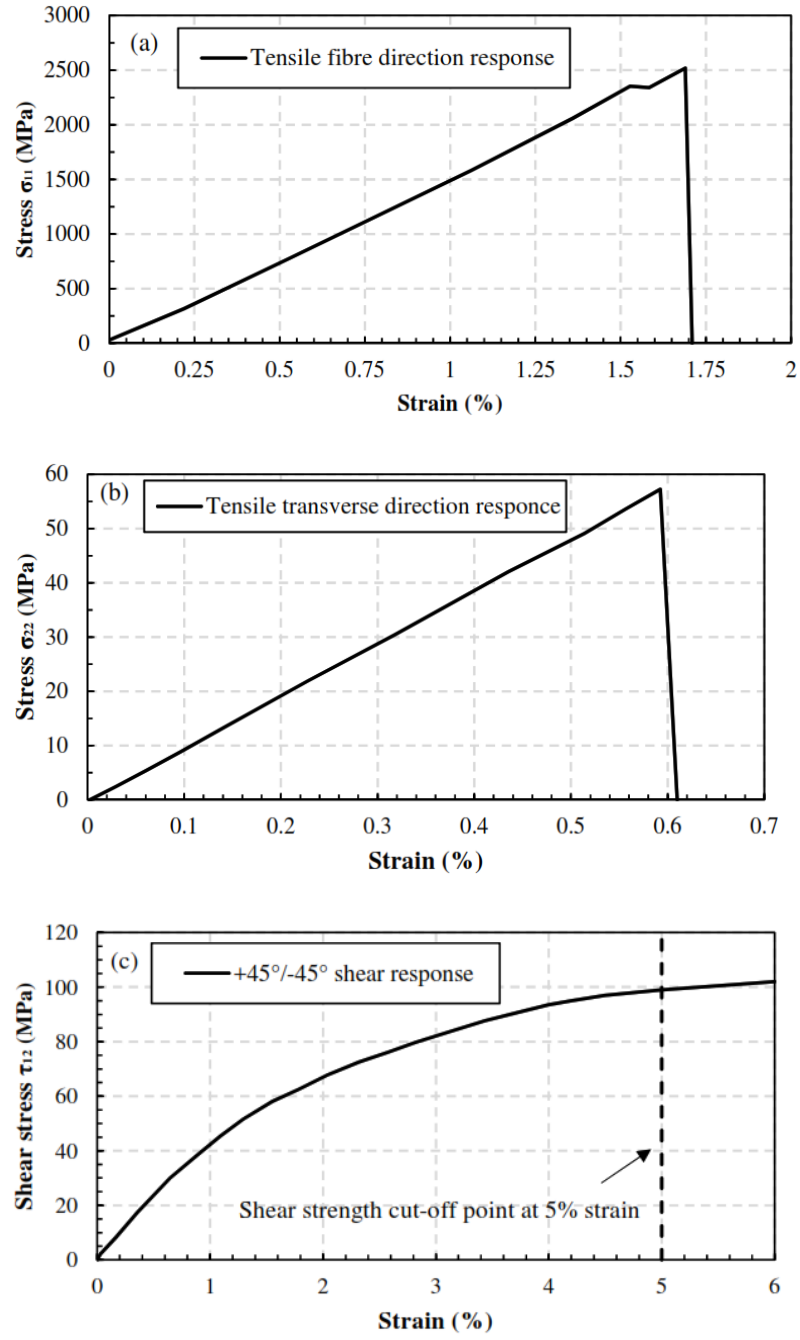


Fig. 2. Stress-strain curves determined from the experiments.

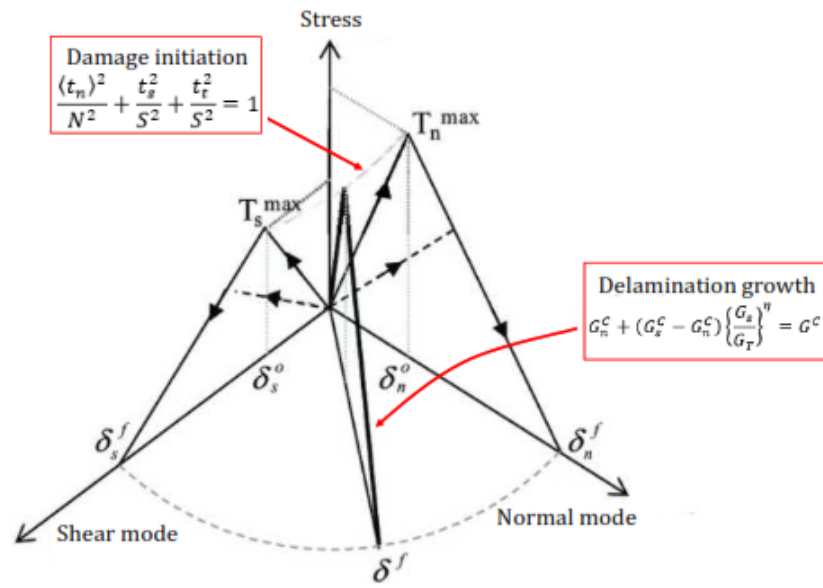
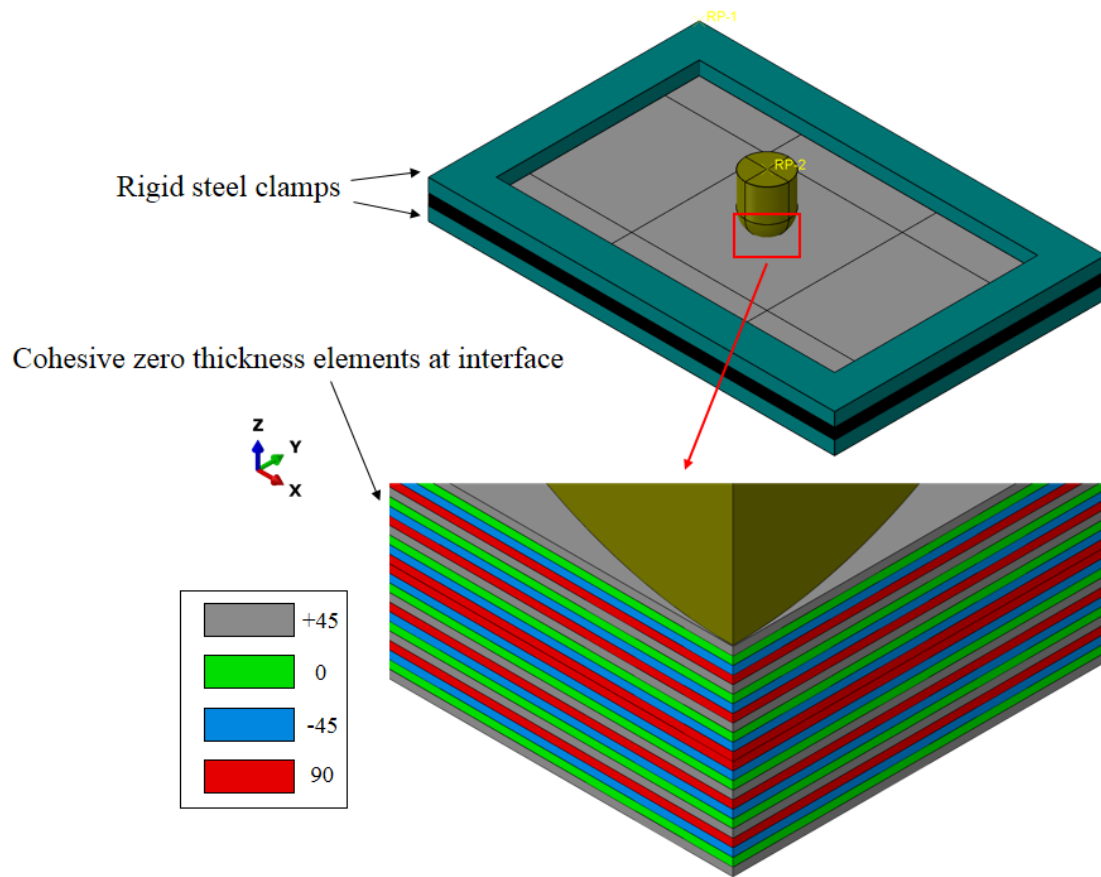


Fig. 3. Mixed-mode response in cohesive interface.



810

Fig. 4. Finite element model for the impact analysis with stacking sequence of the laminate.

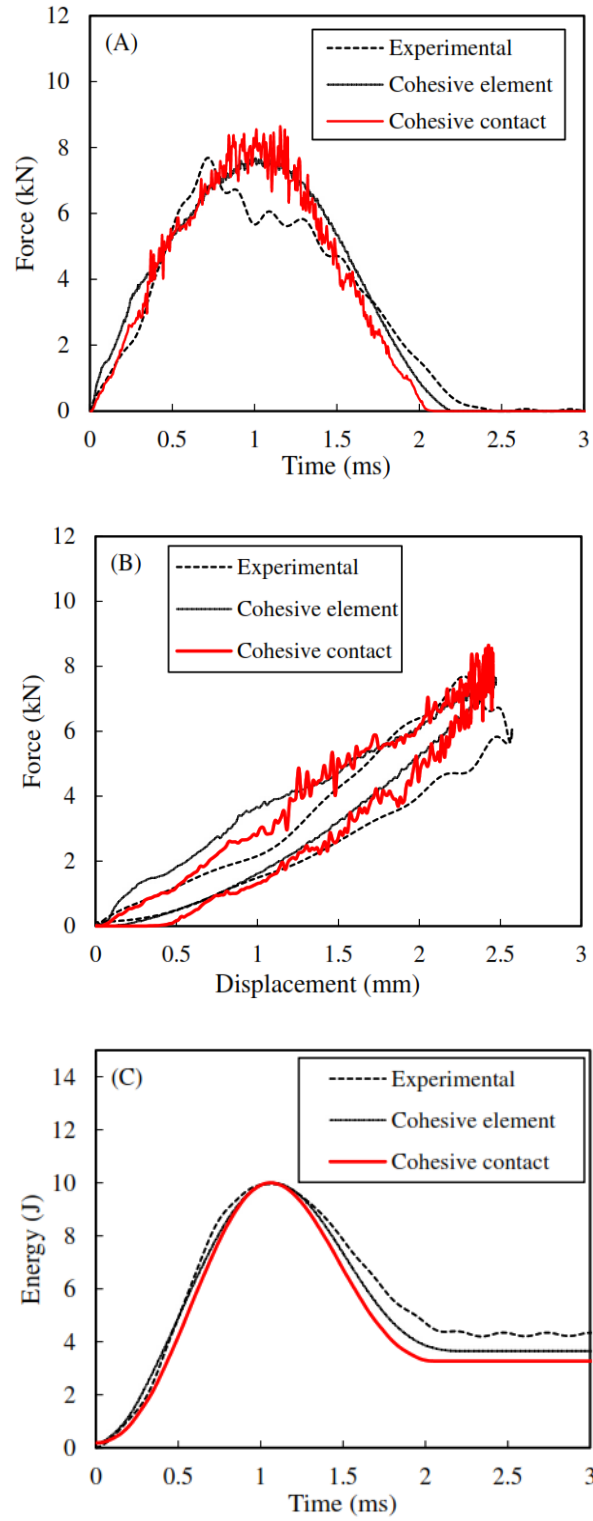


Fig. 5. Comparison of experimental and numerical results for impact energy of 10J: (a) force-time histories, (b) force-displacement curve, and (c) energy-time histories.

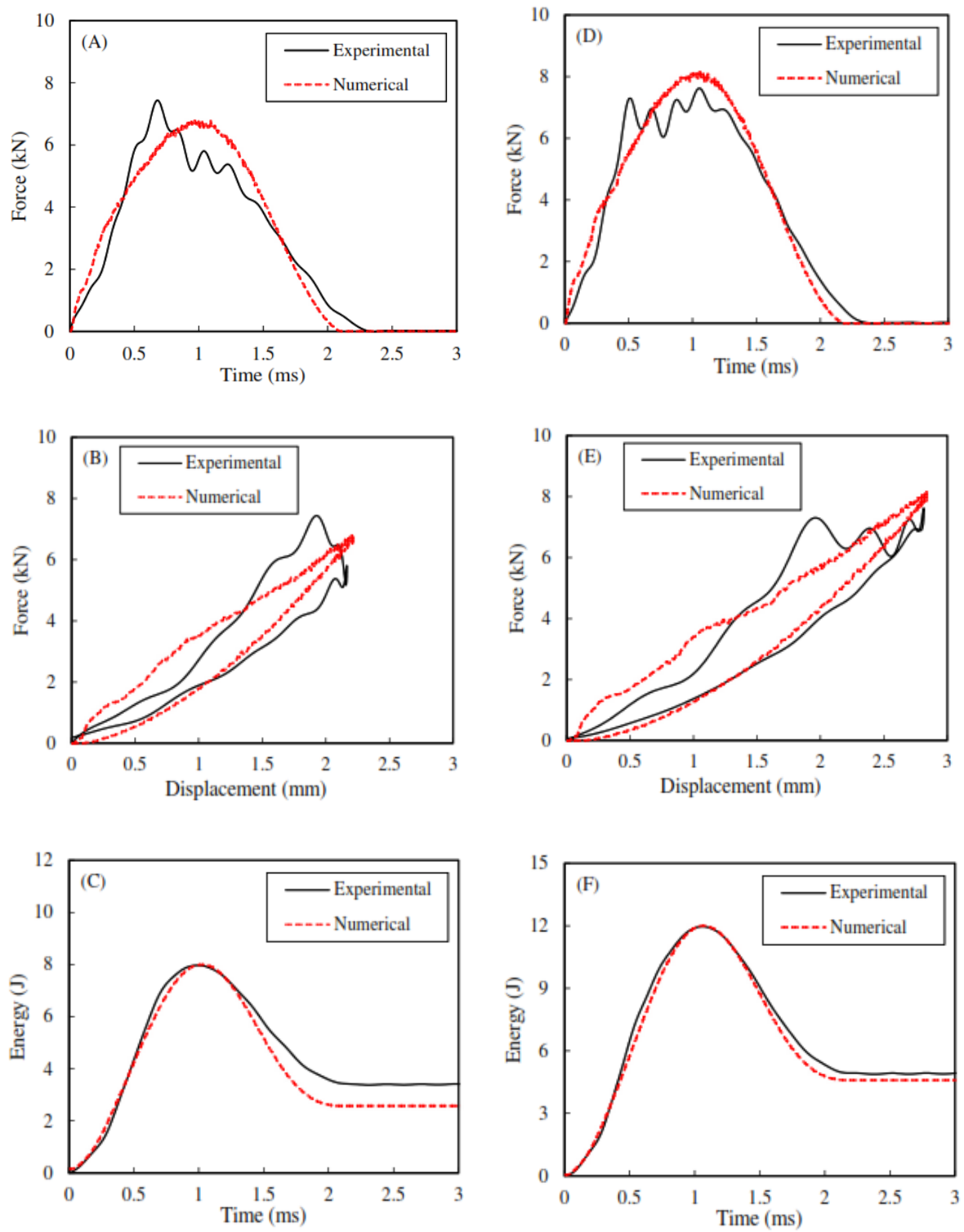


Fig. 6. Comparison of experimental and numerical results of, (a) force-time histories, (b) force-displacement curve, and (c) energy-time histories, at 8 J (on the left) and 12 J (on the right) impact energy levels.

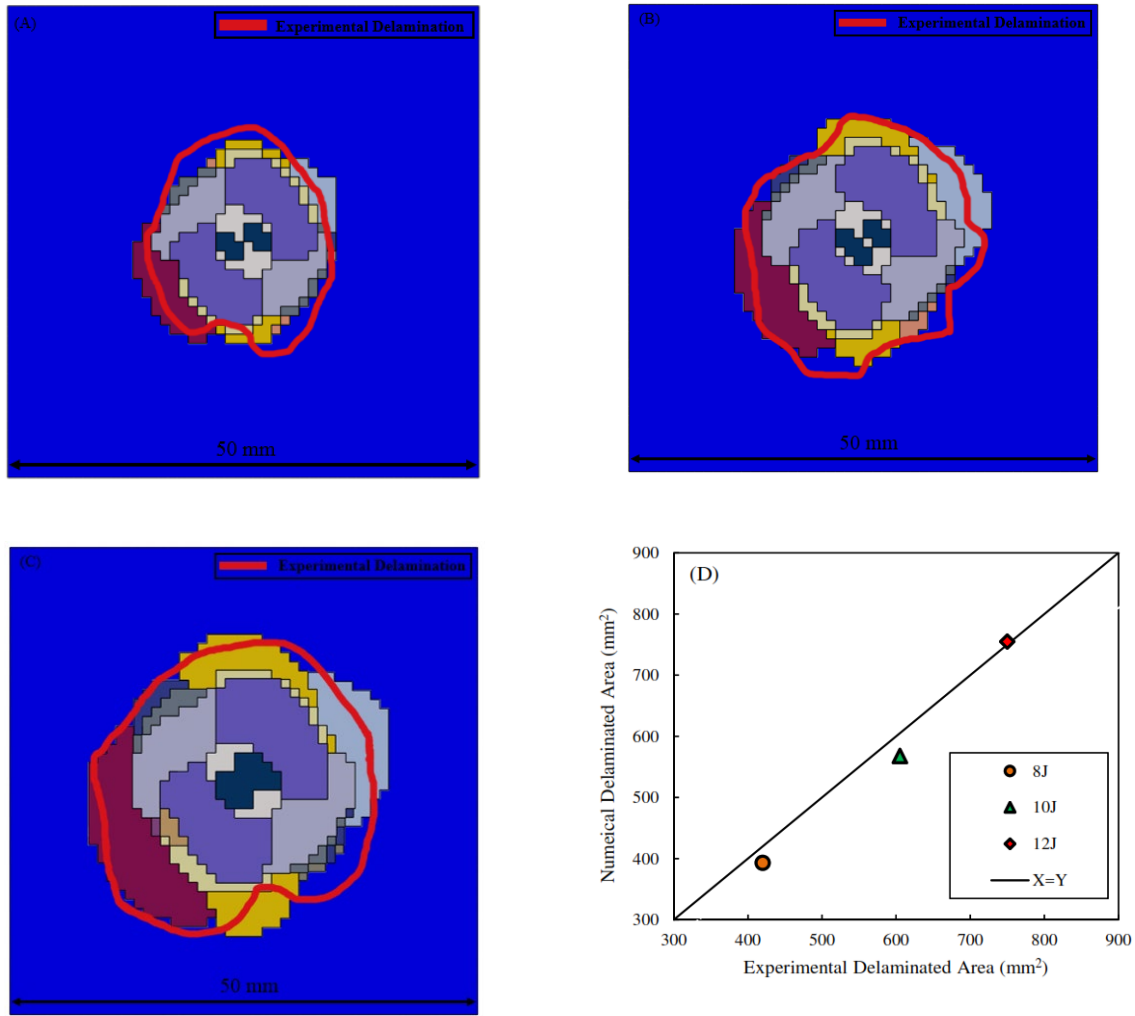


Fig. 7. Comparison between experimental and numerical delaminated area at (a) 8 J, (b) 10 J, (c) 12 J, and (d) summary of delaminated area at each impact energy.

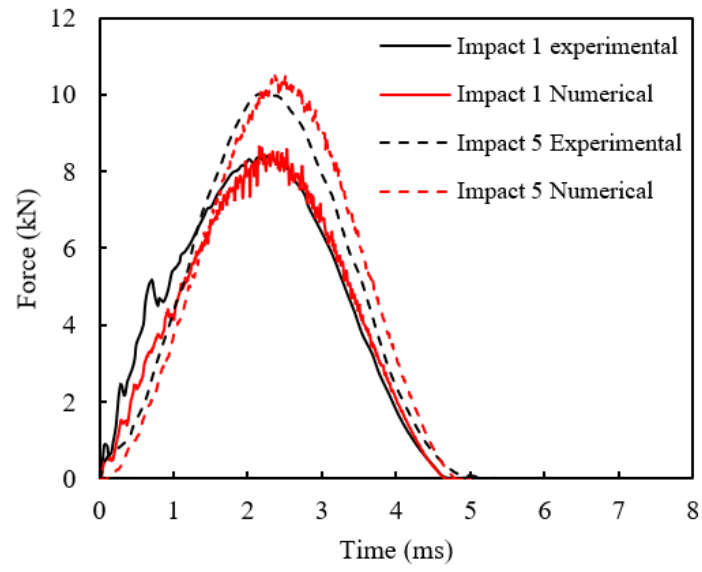


Fig. 8 Comparison of the numerical force displacement of repeated impacts with the experimental curves of Ref. [50]

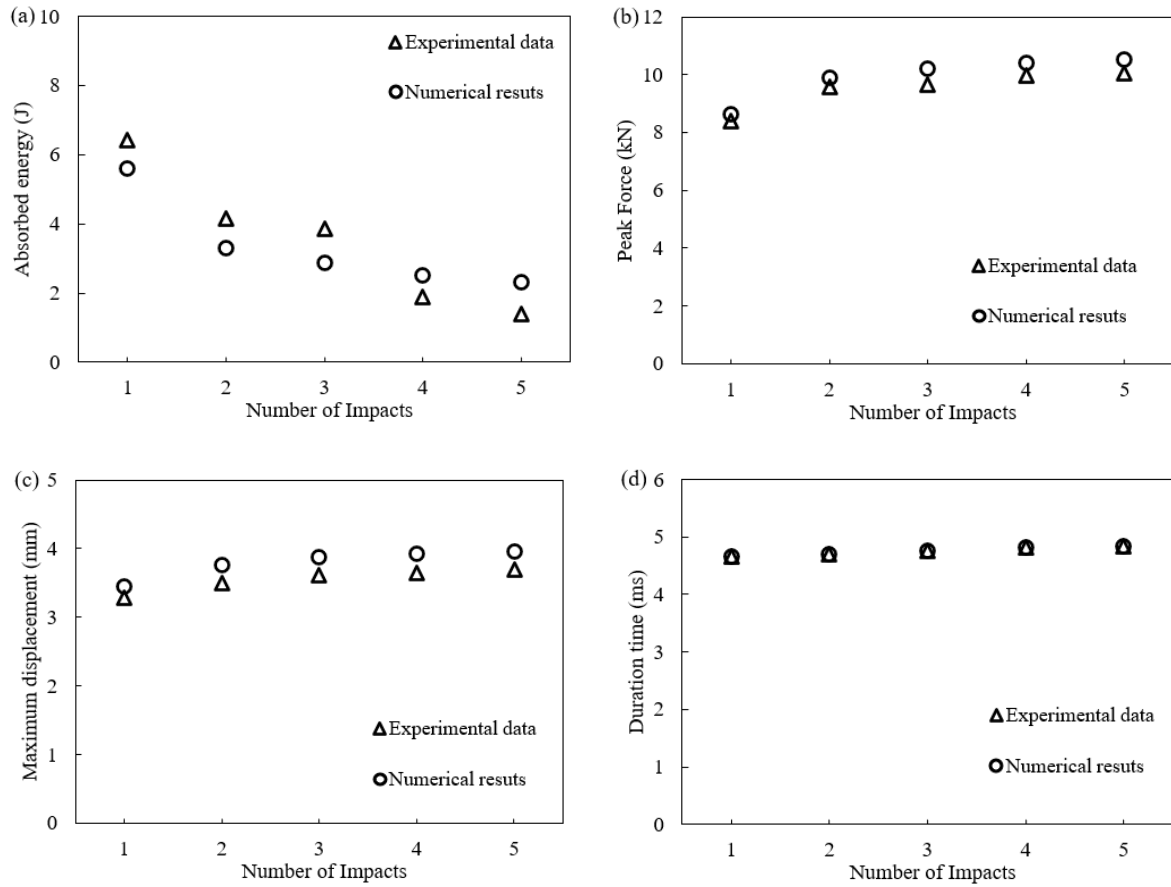


Fig. 9 Comparison of the numerical with the experimental results of Ref. [50], (a) peak force, (b) Absorbed energy, (c) Maximum central displacement, and (d) time duration.

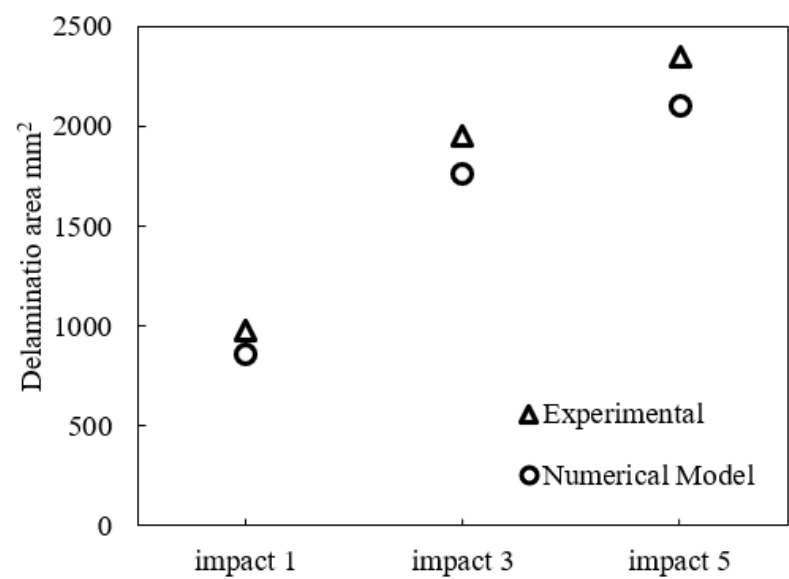
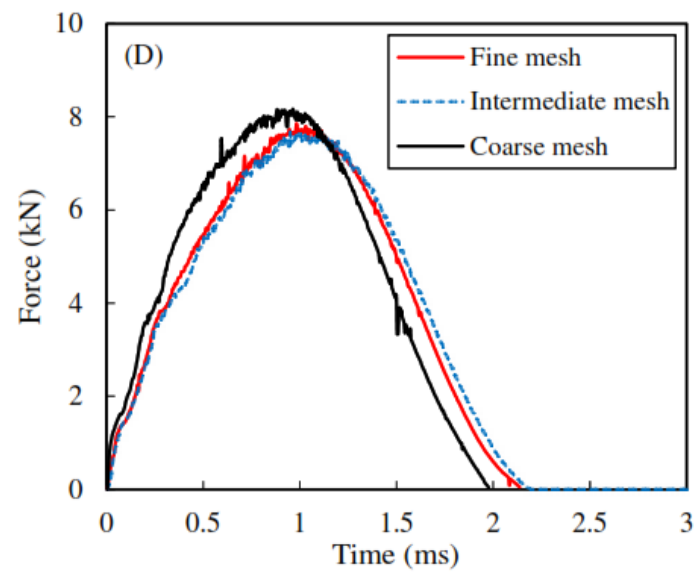


Fig. 10 Comparison of the numerical results of delamination with the experimental results of Ref. [50].



835 Fig. 11. Force-time history simulation results of different element sizes.

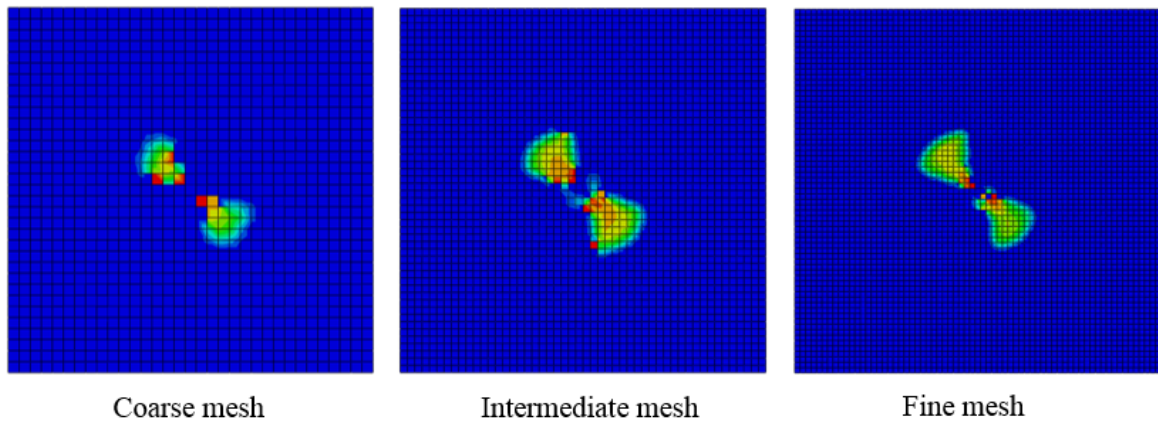


Fig. 12. Effect of element size in the prediction of matrix damage in a typical ply.

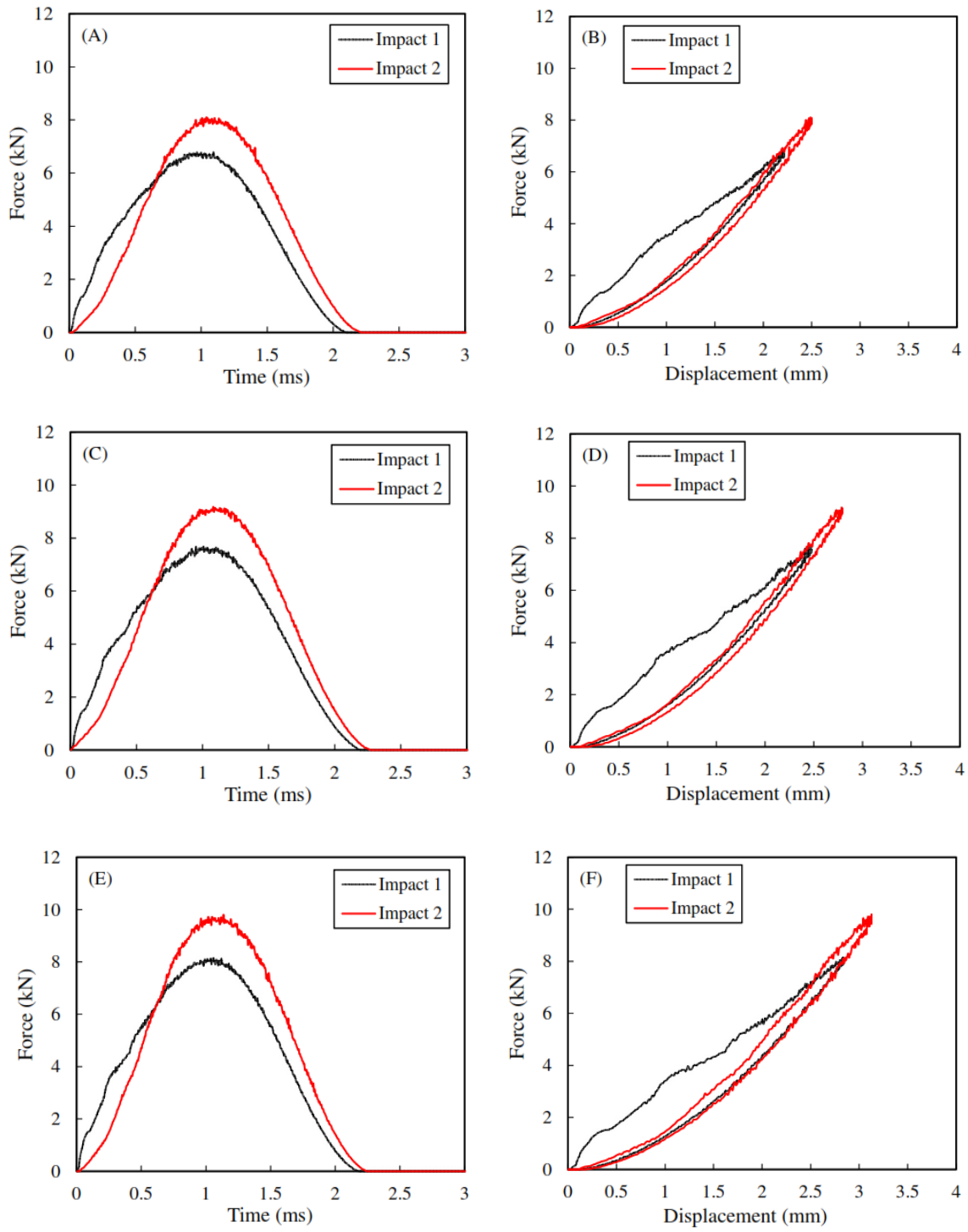


Fig. 13. Comparison between the numerical results obtained for the force-time histories and force displacement curve from the first and second impacts, (a, b) 8 J, (c, d) 10 J, and (e, f) 12 J.

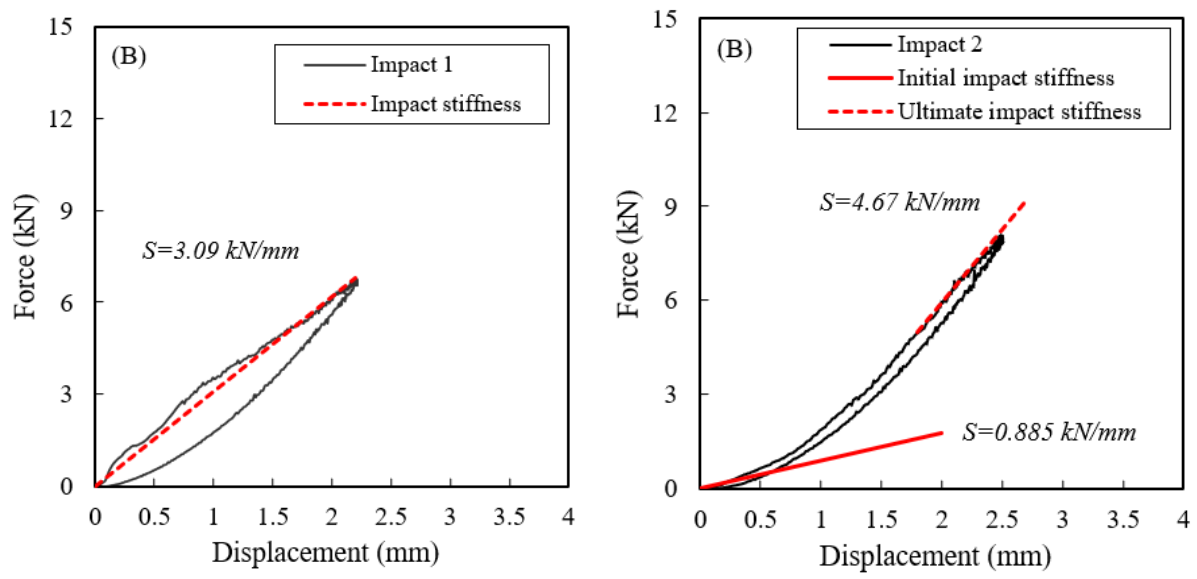


Fig. 14. Impact bending stiffness for the first and second impacts.

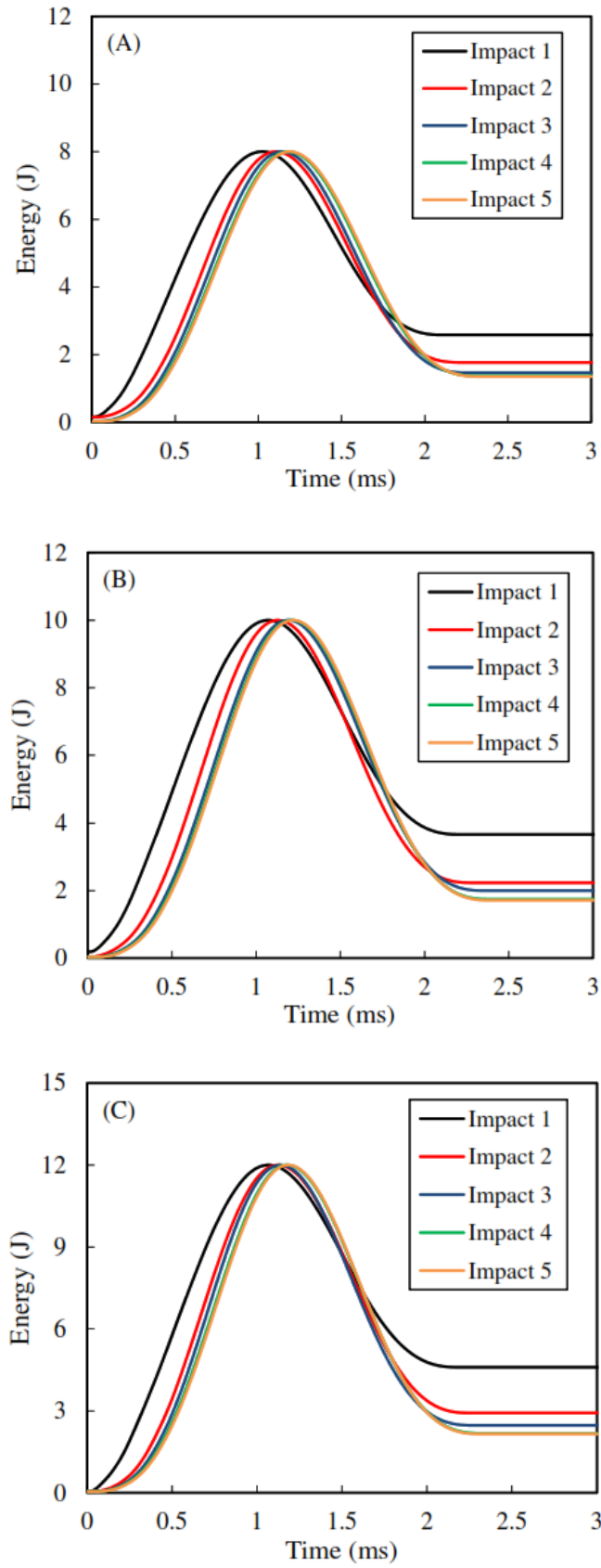


Fig. 15. Energy time histories of five sequential impacts at energy levels of: (a) 8 J, (b) 10 J, and (c) 12 J.

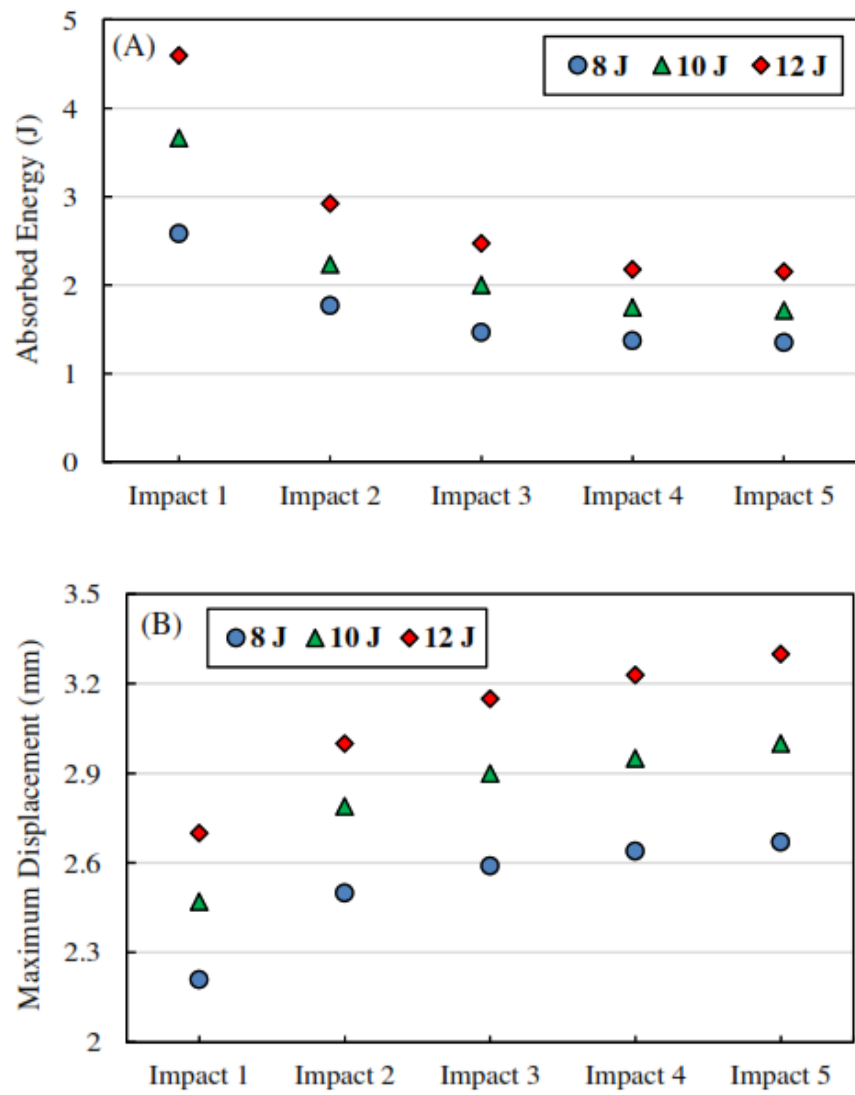


Fig. 16. Summary of (a) absorbed energies, and (b) maximum displacements that are resulted from five repeated impacts at different energy levels.

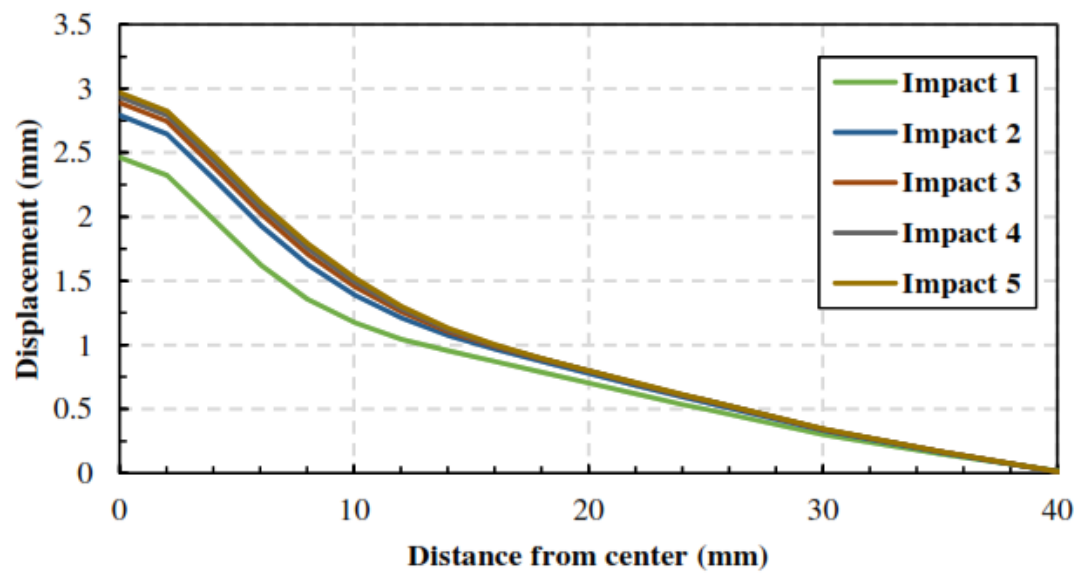


Fig. 17. Transient plate deformation for five impact events of 10 J.

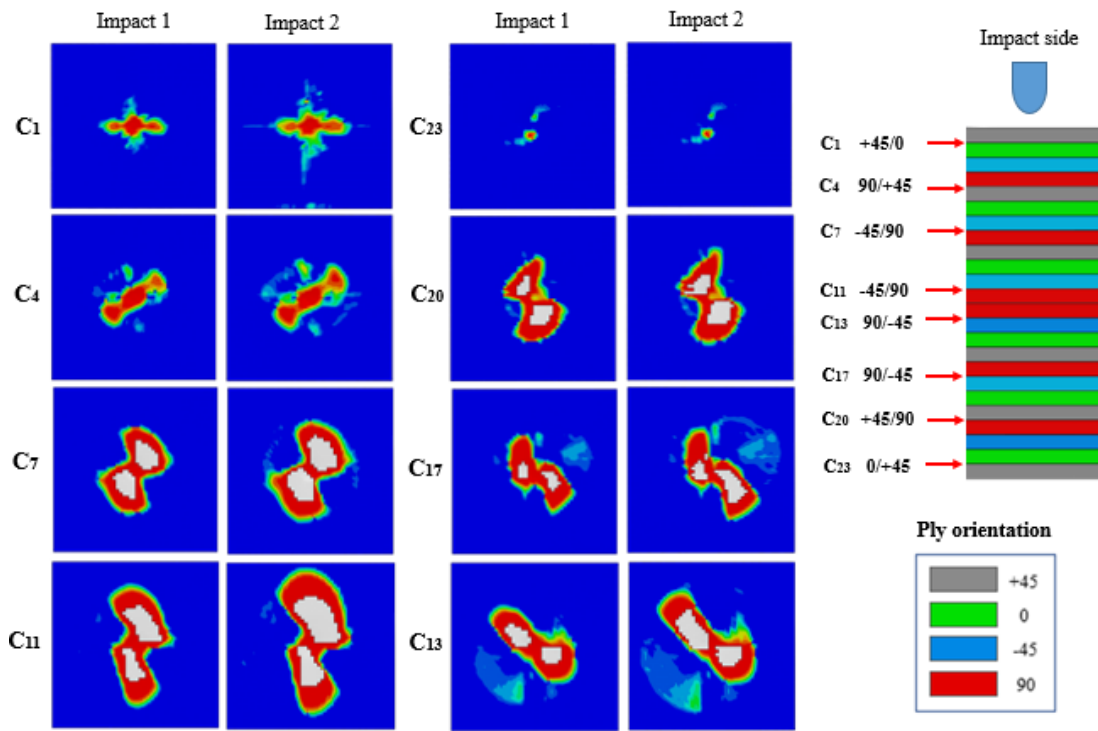
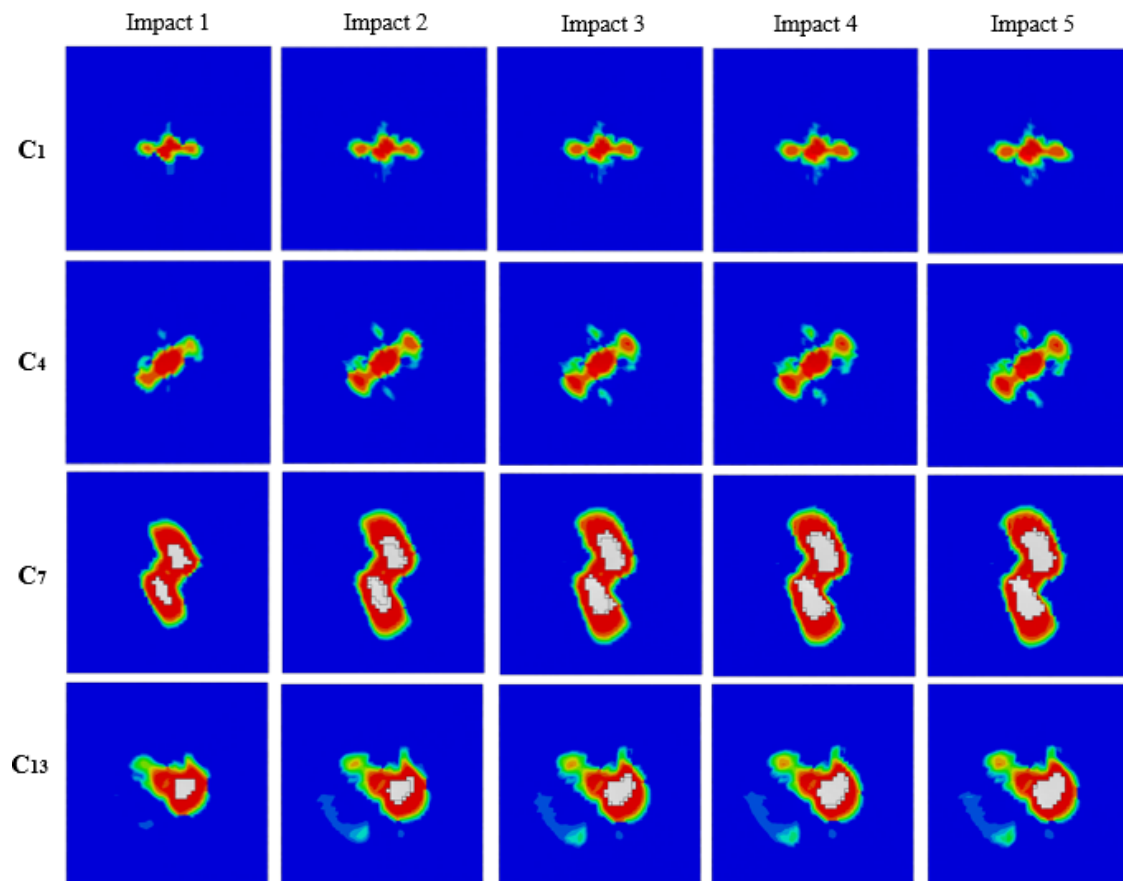


Fig. 18. Comparison of the delaminated area at selected interfaces through the thickness of the laminate after first and second impacts at 12 J impact energy level.



860 Fig. 19. Comparison between the delaminated areas at selected interfaces through the thickness of the laminate after five repeated impacts at 8 J impact energy level.

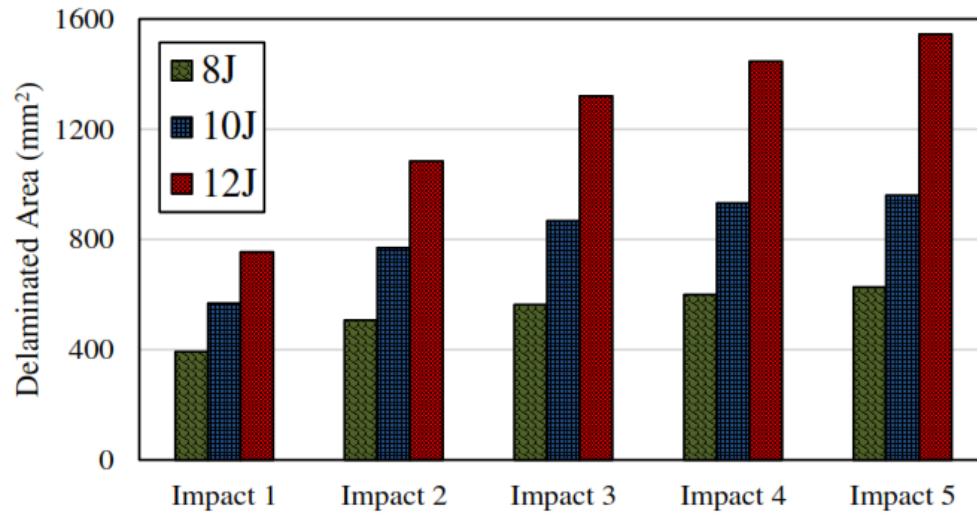


Fig. 20. Summary of results of total delamination area at three impact energy levels of 8 J, 10 J, and 12 J.

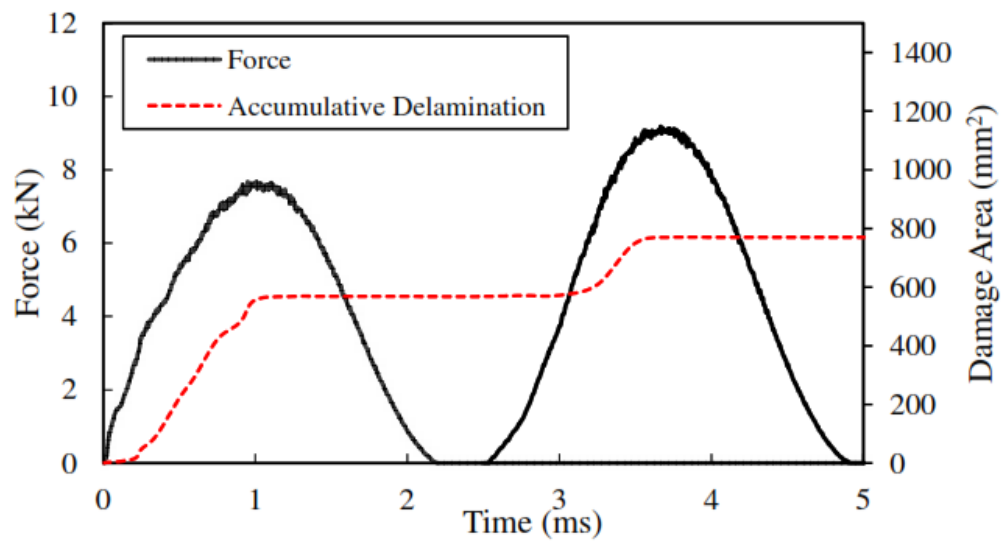


Fig. 21. Comparison of force-time and damage-time histories of the first and second impacts at 10 J impact energy.

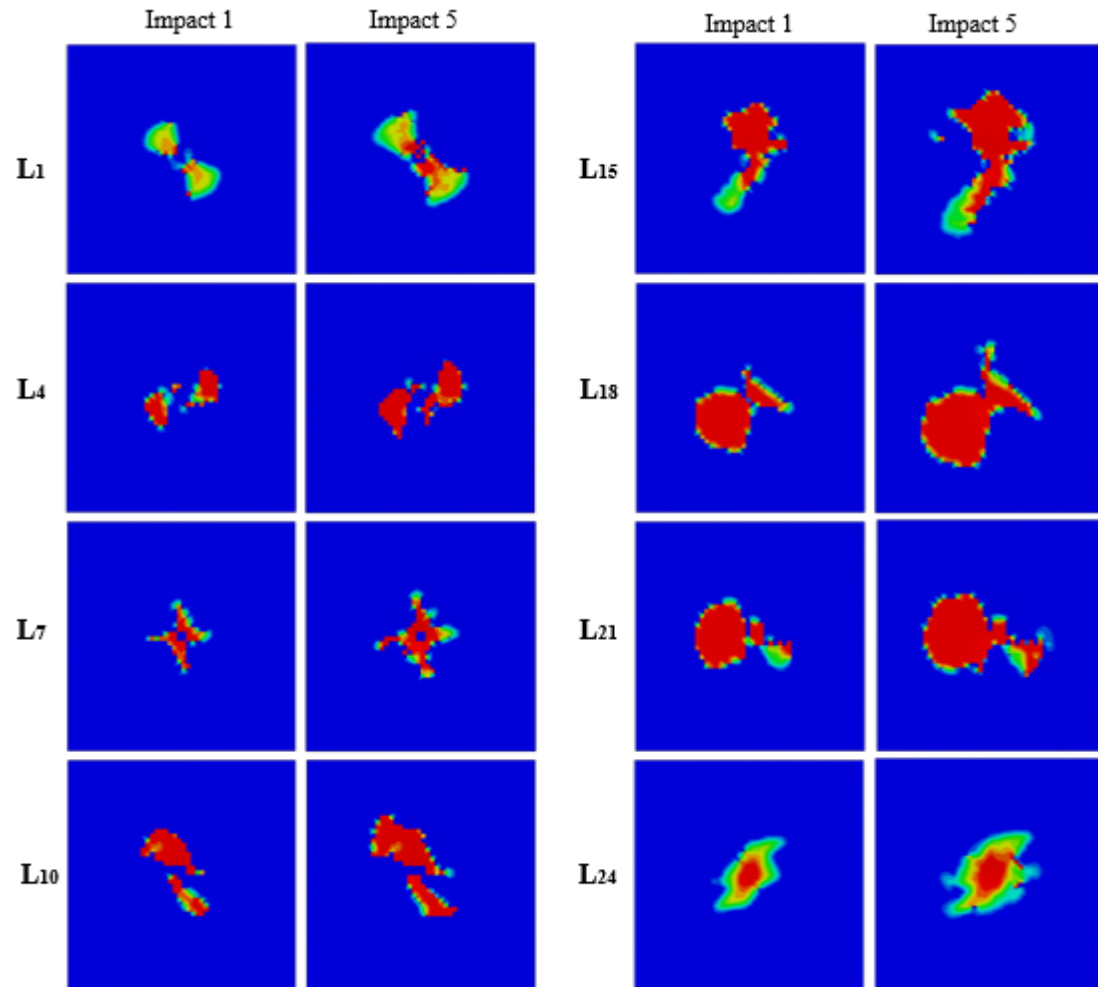
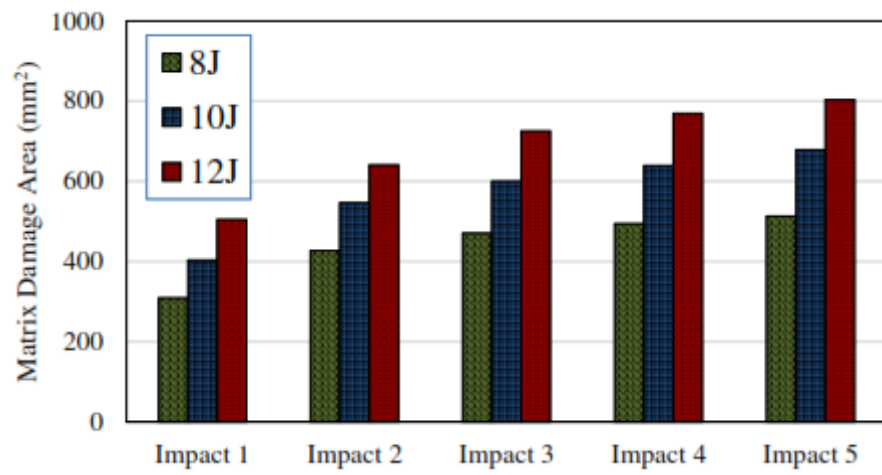


Fig. 22. Comparison of predicted matrix damage at selected plies for first and fifth impact at the impact energy of 10 J.



875 Fig. 23. Summary of the results of the tensile matrix damage area at three impact energy levels of 8 J, 10 J, and 12 J.

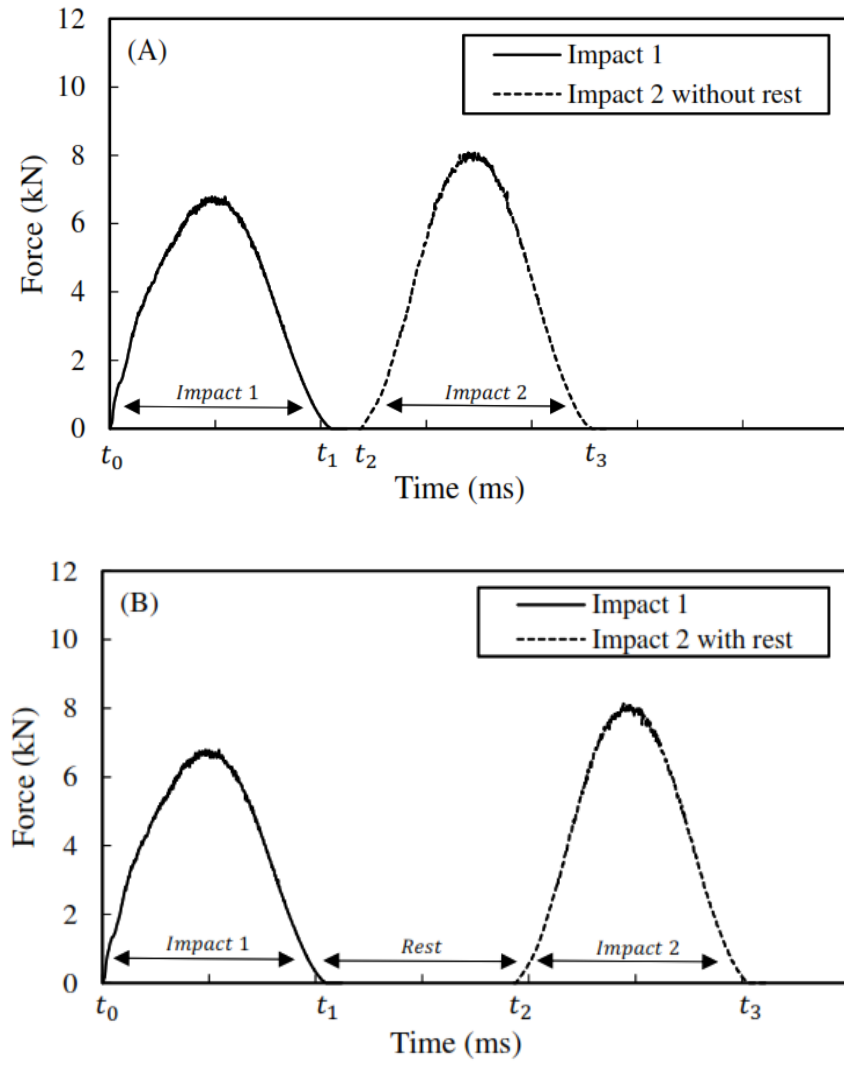


Fig. 24. Force-time histories of models with rest step and without rest step for impact energy of 10 J.

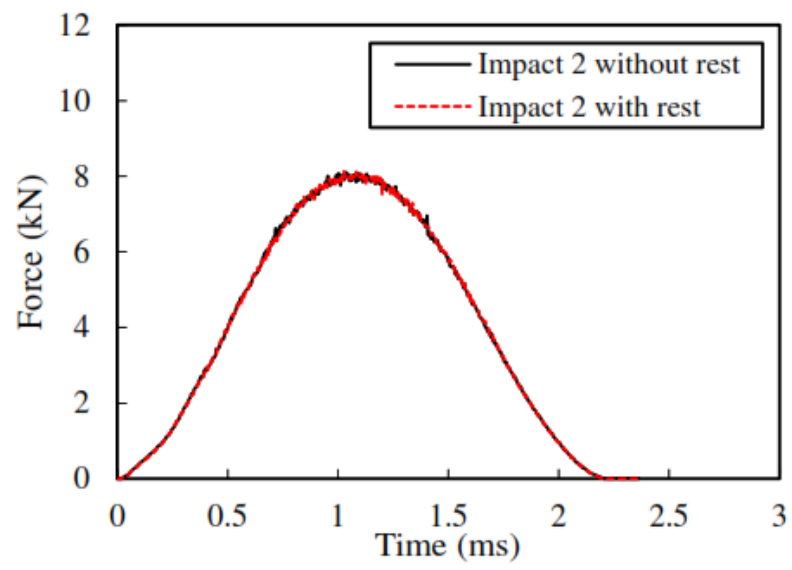


Fig. 25. Comparison of the second impact response of models with and without rest step.