Failure prediction of T-stiffened composite panels subjected to compression after edge impact

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Abstract

Low-velocity impact on the free edge of composite stiffener is considered as a critical factor on the loss of compression strength. This paper proposes a [phenomenological-based](#) mechanical finite element model for the Compression-After-Edge-Impact (CAEI) failure prediction of the composite panel stiffened with T-shaped stiffeners, of which the web is subjected to edge impact damage. Based on the specific failure mechanisms during an edge impact, localized crushing failure, which is insignificant during a classic skin impact, however is considered as a key point of the proposed model for the simplification of impact-induced damage. With the help of a composite damage model comprising both continuum damage mechanics model and surface-based cohesive contact model, a good correlation between the experimental and numerical results is obtained, and therefore validates correctness and effectiveness of the proposed mechanical model. Furthermore, the results reveal that the propagation of fiber compressive failure plays a major role in the compressive failure of the impacted web, of which complete fracture determines the ultimate failure load of the

T-stiffened composite panel.

Keywords: Compression after impact; Damage tolerance; Failure mechanism; Residual strength; Finite element analysis (FEA)

1. Introduction

Stiffened composite panels are commonly used in primary structures to obtain weight reduction in case of high bending stiffness and buckling resistance.

Unfortunately, their usable design strain level is still suppressed by high impact susceptibility of composite materials. Due to the fact that drastic reductions of structural load-carrying capability will be caused by impact-induced damage which might remain undetected by visual inspection [1], aeronautical industry certification requires the proof of impact resistance depending upon the impact damage detectability.

Generally, as a result of foreign tools being dropped during manufacture or maintenance operations, impacts at the free edge of a T-shaped stiffener are likely to introduce more complicated damage modes than classic skin impacts, due to the fact that the in-plane loading during edge impacts might cause fiber failure, which is of minor importance in case of skin impact damage induced by transverse loading, to be important. In addition, residual properties of stiffened panels will be significantly reduced because a substantial portion of the load is carried by stiffeners [2]. It makes sense to investigate in detail the edge impact phenomenon and particularly corresponding Compression-After-Edge-Impact (CAEI) failure mechanisms, in order to identify the design parameters that affect structural residual strength.

Recently, classic composite skin impact issues and relevant CAI failure mechanisms have been fairly well developed, both theoretically and experimentally [3-7]. The key compressive failure mechanisms of impacted laminates can be divided into two distinct phases: the evolution of the damage distributing region near the impact damaged location and the catastrophic failure of the laminate. Delamination is considered as a critical factor to the loss of stability of the sublaminates and subsequent stress redistribution that lowers residual strength. At the same time, a stress concentration in the boundary of the damage region is responsible for the propagation of internal cracks.

However, previous work has typically been restricted to the study about skin impacts that are normal, transverse impact away from an edge. Compared to the research effort associated with CAI issues, fewer studies have been reported on the damage tolerance about free edge impacts, and particularly, on the further analysis of the CAEI failure mechanisms. Malhotra [8] studies experimentally the effect of impact position for low-velocity impact response and its correlation with residual strength. Rhead et al. [9] presents a semi-analytical fracture mechanics model through assuming that the final failure of composite laminates is caused by local buckling. More recently, inspiring experimental investigations are conducted by Ostre and Bouvet [10, 11] to elaborate the damage scenario of T-stiffeners in both edge impact and ensuing compression experiments. The results show that the edge impact response of the composite laminate is more similar with the progressive crushing process, rather than a skin impact. The propagation of compressive fiber failure is considered to determine the

mechanisms that drive the laminate residual strength after edge impact. Furthermore, a unique model, named "Discrete Ply Model" (DPM) is established to simulate edge impact and CAEI of stiffened composite structures and has achieved acceptable prediction [12, 13]. However, it is computationally demanding that interface elements are expected to be defined between neighboring volume elements in one ply (to simulate matrix cracking) and also between two consecutive plies (to simulate delamination). It seems not yet possible to apply the DPM as a design tool for realistic structures with more complex geometric configurations.

One of main objectives in the present paper, as an extension of the previous authors' experimental investigation [2] into the CAEI behaviour of wing-relevant stiffened composite panels, is to study the compressive failure mechanisms of the CAEI from the numerical point of view. In order to improve CAEI knowledge, a phenomenological-based mechanical method has been proposed to evaluate the residual strength and elaborate the post-impact compressive failure mechanism of the T-stiffened composite panel subjected to free edge impact. In particular, complex damage induced by edge impact is able to incorporate into the numerical CAEI model through a novel simplified approach that makes an analogy with composite crushing failure at the impacted region. Finite element analyses have been carried out and the results are compared to the CAEI experiments to validate the FE model presented in this paper.

2. CAEI test method

2.1. Material properties and specimen configurations

Each test specimen is a flat panel stiffened by three stiffeners with T-shaped cross

section along the longitudinal direction, as shown in Fig. 1(a). In order to ensure the uniform loading and avoid brooming at loading ends, both upper and lower ends of the stiffened panel are reinforced by epoxy resin blocks with steel frames in four sides and also the top of each block. The overall dimensions of stiffened composite panels are illustrated in Fig. 1(b). The T-stiffened panel has a nominal width (W) of about 505 mm and a nominal length (L) of about 790 mm. However, the reinforced blocks, of which the height is 115mm, reduce the effective length to 560 mm. The stiffeners are symmetric with respect to the longitudinal symmetry axis, and equally spaced with an interval (D) of about 145 mm across width.

Three test configurations, referred to as Panel A-T, Panel B-T and Panel C-T, are designed. Four specimens for each configuration are manufactured for the edge impact and subsequent CAEI tests. Fig. 1(c) gives the cross section of one stiffener and skin. All specimens are designed with identical stiffener web height ($h = 60$ mm) and same flange width ($b_1 = 70$ mm), although the thickness and stacking sequences are different for the main items of each specimen type. For Panel A-T, B-T and C-T respectively, corresponding to the skin thickness (t) of 4.81 mm, 5.92 mm and 7.77 mm, and the nominal thickness of the web (t_1) is 7.40 mm, 7.40mm and 10.36 mm, while that of the flange (t_2) is 2.59 mm, 3.33 mm and 4.44mm.

The material of the skin and stiffener is graphite fiber reinforced epoxy composite CYCOM IMS/X850, of which the basic mechanical properties are reported in Table 1. The thickness of single ply is 0.185 mm, and the following different stacking sequences of all the three test configurations are defined:

Panel A-T: $[45/90/-45/0/-45/0/45/0/45/0_2/-45/90]_s$ for the skin of 26 plies,
 $[45/-45/0/-45/0/45/90]_s$ for the stiffener flange of 14 plies, and
 $[45/-45/0/-45/0/45/90_2/45/0/-45/0/-45/45_2/-45_2/0/45/0]_s$ for the stiffener web of 40 plies;
Panel B-T: $[45/90/-45/0_2/-45/0_2/45/0_2/45/0_2/-45/90]_s$ for the skin of 32 plies,
 $[45/0/-45/0/-45/0_2/45/90]_s$ for the stiffener flange of 18 plies, and
 $[45/0/-45/0/-45/0_2/45/90_2/45/0_2/-45/0/-45/0/45_2/-45]_s$ for the stiffener web of 40 plies;
Panel C-T: $[45/90/-45/0_3/-45/0_3/45/0_3/45/0_3/-45/0/90]_s$ for the skin of 42 plies,
 $[45/90/-45/0_3/-45/0_3/45/0]_s$ for the stiffener flange of 24 plies, and
 $[45/90/-45/0_3/-45/0_3/45/0_2/45/0_3/-45/0_3/-45/90/45_2/-45_2/45]_s$ for the stiffener web of 56 plies.

The choice of above stacking sequences is based on the relevant laminating design experience [23], which suggests that stacking sequences should be symmetrical and balanced (equal number of 45° and -45°) to simplify the manufacturing, and to minimize spring back and warping during manufacturing. In order to counteract Poisson's ratio effects, each composite laminate has fiber in the four different orientations, and meanwhile outside 45° plies are used to improve the stability and impact resistance. Besides, due to the stiffened panels mainly submitted to compressive loading, the number of plies at the 0° direction is appropriately increased.

2.2. CAEI experimental set up

Before the CAEI experiments, the free edge impact is introduced at the through-thickness centre of the T-shaped stiffener web, as illustrated in Fig. 1(a), with the application of a vertical drop-weight impact instrument, which can set the required

impact energy by adjusting the mass or the height of a hemispherical impactor in the low-velocity impact experiments. It is noticed that the impactor with smaller diameter could be much easier to impact on the through-thickness center of a T-shaped stiffener in the experimental operation. Hence, based on the recommendation that the smallest size of an impactor should be at least 0.5 inch (12.5 mm) diameter [24], the diameter of impactor, manufactured from steel, is selected as 12.5 mm.

Then, the actual impact energy can be recorded precisely with the incident velocity measured by the velocity measuring device. The nominal values of edge impact energy levels are identified as 32.2J, 27.3J and 39.7J for Panels A-T, B-T and C-T, respectively. Only a brief recall is done here and the interested reader can find more details about the method of determining levels of edge impact energy in [2]. Impact damage in such representative locations is evaluated by nondestructive detection technology with the application of a portable C-scan device called IUCS- II .

Uniaxial compression tests are conducted on a hydraulic axial test machine called YES-W5000F with a maximum 5000 kN load sensor accurate at 1.0% on the test range. During the test, the upper platform is locked by spherical hinge connection at the center, while the lower platform can be moved in a controlled speed with hydraulic actuator. Meanwhile, longitudinal edge supports with blade shape are applied at two lateral sides of the skin to avoid unintended buckling. Fig. 2(a) presents the specimen installed on the test machine.

In the CAEI test phase, the axial load is incremented up to failure, when strain levels are monitored by means of back-to-back strain gauges at each load increment.

The general configuration of the strain gauges is shown in Fig. 2(b). There are 102 strain gauges (G1-G102) manufactured by Zhonghang Electronic Measuring Instruments Company, including 36 rosette strain gauges, bonded back-to-back on both sides of the specimen, in order to monitor strain levels. The data acquisition of the test strain is performed using a strain measure system DH3816 manufactured by Jiangsu Donghua Testing Technology Company.

3. Edge impact-induced damage modeling strategy

To the author's knowledge, nowadays, only a discrete method, proposed by Ostre and Bouvet [12], can be a good substitute for expensive experiments to some extent, in order to evaluate the effect of impact damage severity on the damage tolerance of composite laminates after edge impact. However, due to the fact that non-thickness cohesive elements are required to be inserted in-between interfaces of volume elements, both horizontally and vertically, the simulated interface amounts may be a large obstacle of CAEI analyses for composite aeronautical structures with complex configurations. That is why a phenomenological-based FE model has been proposed with a specific method inspired from the limited measurement data of current edge impact experiments.

Indeed, it must be admitted that the proposed method, which is based on the measurement rather than numerical prediction of the impact-induced damage, depends heavily on the measured results of edge impact experiments and will inevitably bring in some artificial hypothesis about the post-impact damage status. In contrast, accurately predicting the CAEI behaviour of composite structures, through considering overall impact and CAI failure process in the numerical analysis [12, 14], seems to be a more

reasonable method. But several disadvantages associated with this overall-process analysis method are in order.

(1) The initiation and propagation of delamination at each interface between two consecutive plies should be taken into account in the numerical simulation of edge impact tests, because all interfaces are at risk of delamination under impact loading. However, in the modeling of subsequent compression failure, some studies [18, 25] have shown that those interfaces with small delamination areas do not significantly affect the CAI response. It indicates that the application of this overall-process analysis method, which needs cohesive elements to be placed precisely at each interface in the simulations of both edge impact and CAEI, is computationally expensive, due to the large number of composite plies, cohesive interfaces and contact definitions. (2) The oscillations due to impact will make the modification of the CAI boundary conditions difficult. (3) In the overall-process analysis method, the predictive capability of a CAEI model depends heavily on the numerical results obtained by the edge impact model. However, the edge impact model is still required to be established and validated by the large number of experimental results including edge impact damage modes, edge impact response parameters (i.e. impact force-displacement curves and force-time histories), and energy transfer. Even though a finite element model of low-velocity edge impact, with consideration of crushing behaviors, has been proposed by Ostre and Bouvet [13], the prediction of edge impact responses is inadequately obtained, due to neglect of a strain rate effect in the behavior law of the fiber failure in compression.

Therefore, due to the fact that investigations on T-stiffener edge impact issues are

still in the preliminary stage, it is feasible to consider the impact-induced damage based on the experimental observation, not on a numerical prediction, in order to avoid negative influence of the immature impact model on numerical CAEI results. Meanwhile, considering the computational efficiency of incorporating edge impact damage into the CAEI model of stiffened composite panels, a simplified method is proposed as below.

For the free edge impact of T-shaped stiffeners, even if the failure modes generated during an edge impact are similar to those developed during a classic skin impact, such as matrix cracking, fiber breaking and delamination, some distinct phenomena like wedge effect, multiple matrix cracks along the length direction and semi-elliptical delamination area can also be detected in case of edge impact damage (Fig. 3(a)). Furthermore, irrespective of impact energy levels and stacking sequences, the impact force-displacement curves, measured during the edge impact experiments, are relatively similar to the typical ones observed in the progressive crushing of composite laminates [10, 11]. It can be seen from Fig. 3(b) that in the typical force-displacement curve of the T-stiffener under edge impact, a peak force is conventionally observed at the preliminary stage of edge impact. Whereafter, the impact process turns into progressive stage that can be clearly characterized by an approximately constant force with relative oscillations. The specific occurrence of the debris wedge (for the damage morphology) and the force plateau (for the structural response) indicates that the physical controlling mechanism in the edge impact event is sufficiently like the one arising from the progressive crushing process, rather than a skin impact. Hence, in contrast to the

common approach that uses elliptical inclusions to represent stiffness reductions caused by the skin impact [3], a new method is required to be developed to incorporate edge impact damage, especially by taking into account the influence of crushing-related failure on the residual strength.

Crushing-related failure might significantly weaken the load-carrying capacity of the stiffened composite panel due to the fact that a great deal of fiber shear fracture, packs of fibers, powder-like debris and kink bands have formed in the crushing zone. In the edge impact experiments, the main direction of impact loading is along the z direction (see in Fig. 4), while that of compressive loading is changed into the x direction during the CAEI experiments. From the SEM observation of crushed surface [16], packs of fibers and powder-like debris are clearly observed in 90° plies (parallel to the direction of edge impact loading), which indicates that no compressive load is able to be sustained due to totally broken fibers in the crushing zone. For the crushed surface of 0° plies (perpendicular to the direction of edge impact loading), a great deal of debris wedge and shear fracture of the resin can be observed, and might cause the inability to sustain most of the compressive load, because no support to fibers can be provided by the sufficiently damaged resin in the crushing zone. Accordingly, based on the assumption that no load is able to be sustained, the crushing failure zone is simplified as a semi-elliptic hole with geometrical dimensions of the major axis (a), minor axis (b) and depth (D_{crush}), as illustrated in Fig. 4. In order to avoid the rebound effect where the dent relaxes, the length and width, which are approximately equal to the major and minor axes of the dent at the release of impact load, should be measured immediately

after edge impact experiments. The specific value of crushing depth, D_{crush} , which can hardly be obtained through any nondestructive testing approach is estimated as follows.

Neglecting the energy losses for material damping, surface friction, etc, and based on the assumption that all the kinetic energy is transferred into energy absorbed by crushing, the energy balance, prior to the rebound of the impactor, can be written as

$$E_{\text{absorbed}} = \frac{1}{2} m (v_0^2 - v_t^2) = \int_0^{D_{\text{impactor}}^t} F dD \quad (1)$$

where m , D_{impactor}^t , v_0 and v_t are the mass, displacement at t -time, initial velocity and velocity at t -time of the impactor, respectively. F denotes the impact force, and dD is a differential symbol that means an incredibly small variation about the impactor displacement.

It can be noticed that most of the kinetic energy is absorbed by the progressive crushing process, which can be distinguished through a crushing force plateau in the force-displacement curve (see in Figs. 3(b) and 3(c)). In contrast to a classic skin impact, during the edge impact experiments, an interesting phenomenon is observed that regardless of the energy level and stacking sequence, the value of the force plateau is approximately equal to the one multiplying a projected area of the dent by a global mean crushing stress, σ_{mean} [10]. In particular, the specific value of this mean crushing stress, which seems independent on stacking sequences, is found to be approximately equal to the transverse compressive strength of the material [10]. Therefore the force plateau, F_{plateau} , can be calculated by

$$F_{\text{plateau}} = S_{\text{dent}} \sigma_{\text{mean}} \approx \frac{\pi}{4} ab R_{\perp}^c \quad (2)$$

where S_{dent} , a and b are the area, major and minor axes, respectively. $R_{\perp}^c$ is the

transverse compressive strength of the material.

Clearly, the maximum displacement of the impactor (corresponding to $v_t = 0$) can be obtained by integrating over the impactor displacement D from Eq. (1), where the impact force F is actually a function of the impactor displacement D , according to the force-displacement curve measured in the edge impact experiment. However due to the lack of available force-displacement curves, an assumption is made that the progressive crushing process, which can be identified by a relatively constant force, controls the whole edge impact event for long time. Therefore, the maximum displacement of the impactor (corresponding to $v_t = 0$) can be expressed as

$$D_{\text{impactor}}^{\text{max}} \approx \frac{mv_0^2}{2F_{\text{plateau}}} = \frac{2mv_0^2}{\pi abR_{\perp}^c} \quad (3)$$

It is worth emphasizing that the premise to use Eq. (3) is the existence of the force plateau that should be long enough to present a constant level, otherwise the initial slope and the force peak must be taken into account in the energy balance. A conclusion can be draw from the measured force-displacement curves [10] that along with the increase of the impact energy level, the displacement corresponding to the phase of a dramatic change in force remains almost unchanged, while that corresponding to the phase of a constant force keeps getting longer. From the force-displacement curves of composite laminates impacted at 20 J (the impact energy of per unit thickness is 3.33 J/mm), it can be seen that the displacement corresponding to the phase of a dramatic change in force is about 0.5 mm, while that of the phase of a constant force is about 3 mm (see in Fig. 3(c)). Since the lowest impact energy (about 3.69 J/mm for Panel B-T) in this study is higher than 3.33 J/mm and the tendencies of force-displacement curves are similar

whatever the material and stacking, the force-displacement curves could be like the typical ones presented in Fig. 3(c). Hence, for our configurations, it can be inferred, to some extent, that the force plateau is long enough to use Eq. (3), even though no measured impact force-displacement curve is available.

Obviously, the actual depth of crushing zone, D_{crush} , is larger than the maximum displacement of the impactor, $D_{\text{impactor}}^{\text{max}}$, as shown in Fig. 4. In order to obtain the actual depth of crushing zone, the transmission of crushing-related failure is taken into account with the application of an empirical approach proposed in [12, 13, 17]. The crushing failure is assumed to be transmitted between consecutive material points when the compressive deformation of the damaged material point reaches k times of the initial height. Thus, corresponding to the maximum compressive deformation (namely the impactor's maximum displacement), the maximum depth of the crushing zone (D_{crush}) is

$$D_{\text{crush}} = \frac{D_{\text{impactor}}^{\text{max}}}{k} = \frac{2}{\pi k} \frac{mv_0^2}{abR_{\perp}^c} \quad (4)$$

The critical compressive deformation of each damaged material point is actually different. In practice, the present analysis set the coefficient k equal to 0.3 by using an inverse approach that is proposed in the following 'Discussions' section.

In fact, the actual measured shape and size of the delamination zone at each interface is typically more complex. But the delamination areas with smaller sizes are assumed to have little effect on the compression response [18]. Thus, multiple delamination cracks can be approximated by severe delamination failure at critical interfaces. At each application it is supposed that only the maximum delamination being examined is present in the simplified model. Due to symmetry of the geometry and load,

the maximum delamination comes in pairs and is symmetric with respect to the symmetry axis of the cross-section (Fig. 4). Fig. 5 gives the typical post-impact C-scan images and visual inspection of three test configurations at the edge impact locations. The colour bar, on the left of Fig. 5(a), qualitatively represents relative ranges of delamination through thickness, with colours at the top of the bar lying close to the scanned surface of the impacted web. Therefore, the range of maximum delamination, corresponding to the specific colour with maximum coverage in C-scan images, can be obtained by the ratio between colour numbers of the maximum delamination and the web thickness. Since the colour bar only shows relative ranges of delamination positions for each colour, the specific position of the maximum delamination can be eventually measured by inspecting the long cracks at the impacted surface within the range obtained above, as shown in Fig. 5(b). It is worth noting that for Panel A-T or B-T, its web thickness of 7.4 mm is scaled with the number of 100 (represented by dark green), while the web thickness of 10.36 mm is scaled with the number of 140 (represented by yellow) for Panel C-T. By inspecting the projected delaminated area in tests, the area of the edge impact-induced delamination within the sublamine is assumed to be semi-elliptical in shape, with major and minor axes l and w , respectively.

The geometric parameters for modeling edge impact damage in this study are listed in Table 2. All the geometric parameters, except for the maximum depth of the crushing zone (D_{crush}), can be measured after the edge impact experiments. However, the geometric parameter, D_{crush} , can be calculated in Eq. (4). Note that if there is no dent at

really low impact energy, only the edge impact-induced delamination is incorporated in the CAEI finite element model, due to no presence of the crushing zone observed in the experiment.

4. Finite element modeling technique

4.1. Composite damage model

4.1.1. Damage initiation theory based on Puck failure criteria

2D Puck's failure criteria [26] are implemented for predicting the initiation of intralaminar damage. According to the fracture plane's normal stress, the matrix failure is divided into three different **inter-fiber fracture (IFF)** modes, namely Mode A, Mode B and Mode C defined as follows:

Inter-fiber fracture in transverse tension (IFFT in Mode A):

$$\sigma_2 \geq 0 \rightarrow f_{\text{IFF}} = \sqrt{\left[\left(\frac{1}{R_{\perp}^t} - \frac{p_{\perp\parallel}^t}{R_{\perp\parallel}} \right) \sigma_2 \right]^2 + \left(\frac{\tau_{21}}{R_{\perp\parallel}} \right)^2} + \frac{p_{\perp\parallel}^t}{R_{\perp\parallel}} \sigma_2 \quad (5)$$

Inter-fiber fracture in moderate transverse compression (IFFC in Mode B):

$$\sigma_2 < 0 \wedge \left| \frac{\sigma_2}{\tau_{21}} \right| \leq \left| \frac{R_{\perp\perp}^A}{\tau_{21,c}} \right| \rightarrow f_{\text{IFF}} = \sqrt{\left(\frac{p_{\perp\parallel}^c}{R_{\perp\parallel}} \sigma_2 \right)^2 + \left(\frac{\tau_{21}}{R_{\perp\parallel}} \right)^2} + \frac{p_{\perp\parallel}^c}{R_{\perp\parallel}} \sigma_2 \quad (6)$$

Inter-fiber fracture in large transverse compression (IFFC in Mode C):

$$\sigma_2 < 0 \wedge \left| \frac{\sigma_2}{\tau_{21}} \right| > \left| \frac{R_{\perp\perp}^A}{\tau_{21,c}} \right| \rightarrow f_{\text{IFF}} = \left[\left(\frac{\tau_{21}}{2(1 + p_{\perp\perp}^c) R_{\perp\parallel}} \right)^2 + \left(\frac{\sigma_2}{R_{\perp}^c} \right)^2 \right] \frac{R_{\perp}^c}{-\sigma_2} \quad (7)$$

$$\text{with } \frac{p_{\perp\perp}^c}{R_{\perp\perp}^A} = \frac{p_{\perp\parallel}^c}{R_{\perp\parallel}} \text{ and } \tau_{21,c} = R_{\perp\parallel} \sqrt{1 + 2p_{\perp\perp}^c}$$

where $R_{\perp}^t$ and $R_{\perp}^c$ are the tensile and compressive strength against the transverse

direction. $R_{\perp\parallel}$ denotes the longitudinal or transverse shear strength. $p_{\perp\parallel}^t$, $p_{\perp\perp}^t$, $p_{\perp\perp}^c$

and $p_{\perp}^c$ are the inclination parameters of contour lines of the fracture body. Typically, the present analysis followed the recommendation for the specific values of 0.35, 0.30, 0.27 and 0.27 respectively [15, 27].

The fiber fracture (FF) criteria, taking into account the influence of a transverse Poisson's effect, is

Fiber tension fracture (FFT):

$$\varepsilon_1 \geq 0 \rightarrow f_{FF} = \frac{1}{\varepsilon_{1,T}} \left(\varepsilon_1 + \frac{\nu_{f12}}{E_{f1}} m_{\sigma f} \sigma_2 \right) \quad (8)$$

Fiber compression fracture (FFC):

$$\varepsilon_1 < 0 \rightarrow f_{FF} = \frac{1}{\varepsilon_{1,C}} \left| \left(\varepsilon_1 + \frac{\nu_{f12}}{E_{f1}} m_{\sigma f} \sigma_2 \right) \right| + (10\gamma_{21})^2 \quad (9)$$

where $\varepsilon_{1,T}$ and $\varepsilon_{1,C}$ are the tensile and compressive failure strains of the UD laminate in the fiber direction, respectively. E_{f1} and ν_{f12} are Young modulus and Poisson's ratio for the fibers, respectively. $m_{\sigma f}$ is a magnification factor for the transverse stress in the fibers (for GFRP $m_{\sigma f} \approx 1.3$ and for CFRP $m_{\sigma f} \approx 1.1$) [26].

4.1.2. Modelling damage propagation

The continuum damage mechanics (CDM) approach implemented in this work uses a damage variable matrix $\mathbf{M}(\mathbf{D})$ to characterize the severity of damage and effective fractional reduction of load carrying area. The effective stress of the material with micro cracks, based on the damage tensor, can be defined as

$$\bar{\sigma} = \mathbf{M}(\mathbf{D}) \cdot \sigma \quad (10)$$

where in the plane stress condition, $\mathbf{M}(\mathbf{D}) = \text{diag}[1/\omega_{11}, 1/\omega_{22}, 1/\omega_{12}]$, $\omega_{11} = 1 - d_1$,

$\omega_{22} = 1 - d_2$ and $\omega_{12} = (1 - d_1)(1 - d_2)$.

According the strain energy for the intact material, the equivalence strain energy for the damaged material is following

$$W_d = \frac{1}{2} \bar{\sigma} \cdot C_0^{-1} \cdot \bar{\sigma} = \frac{1}{2} \sigma \cdot M(D)^T \cdot C_0^{-1} \cdot M(D) \cdot \sigma = \frac{1}{2} \sigma \cdot C_d^{-1} \cdot \sigma \quad (11)$$

where $C_d = M(D)^{-1} \cdot C_0^{-1} \cdot M(D)^T$ is the damage constitutive of composite materials.

C_0 is the stiffness matrix for plane stress in an intact orthotropic material, and its expression can be found in the reference [28].

A strong mesh dependency of the FE results in that the energy dissipated decreases upon mesh refinement will be caused by strain localization. Due to the fact that the energy dissipated in the damage propagation phase is proportional to the element size, a crack band model [29] is implemented to achieve mesh objectivity. Once a failure criterion has been satisfied, gradual degradation of material properties follows a linear strain-softening law with a negative slope. The equivalent strain at final failure, ε_{eq}^f , can be derived from [20]

$$\varepsilon_{eq}^f = \frac{2G_c}{\sigma_{eq}^0 L^c} = \frac{2g_c}{\sigma_{eq}^0} \quad (12)$$

where σ_{eq}^0 is the equivalent stress at damage initiation. G_c is the critical fracture energy in $N \cdot mm^{-1}$. g_c is the critical fracture energy density smeared in the characteristic length. L^c is the characteristic length that will keep a constant energy release rate per unit area of the crack.

Following a bi-linear constitutive relationship, the internal damage variable, d^I , corresponding to four damage modes at each time step is given by [20]

$$d^I = \max \left\{ 0, \min \left\{ 1, \varepsilon_{eq,I}^f \frac{\varepsilon_{eq,I} - \varepsilon_{eq,I}^0}{\varepsilon_{eq,I} (\varepsilon_{eq,I}^f - \varepsilon_{eq,I}^0)} \right\} \right\} \quad I \in (FFT, FFC, IFFT, IFFC) \quad (13)$$

where $\varepsilon_{\text{eq},I}$, $\varepsilon_{\text{eq},I}^0$ and $\varepsilon_{\text{eq},I}^f$ are the equivalent strains at the current phase, damage initiation and final failure, respectively. Besides, damage irreversibility can be accounted for with the functional form 'max'.

The damage variables associated with intralaminar failure is defined as follows

$$d_1 = 1 - (1 - d_1^{\text{FFT}})(1 - d_1^{\text{FFC}}), \quad d_2 = \max\left(\frac{\langle \sigma_2 \rangle}{|\sigma_2|} d_2^{\text{IFFC}}, d_2^{\text{IFFT}}\right) \quad \text{and}$$

$$d_{12} = 1 - (1 - d_1^{\text{FFT}})(1 - d_1^{\text{FFC}})(1 - d_2^{\text{IFFT}})(1 - d_2^{\text{IFFC}}) \quad (14)$$

where $\langle \bullet \rangle$ is the McCauley operator, defined as $\langle x \rangle = (x + |x|)/2$ for $x \in \mathbb{R}$.

The equivalent stress and strain for IFF and FF failure is given by

$$\sigma_{\text{eq,IFF}} = \sqrt{\langle \sigma_2 \rangle^2 + \tau_{21}^2}, \quad \varepsilon_{\text{eq,IFF}} = \sqrt{\langle \varepsilon_2 \rangle^2 + \gamma_{21}^2}$$

$$\sigma_{\text{eq,FF}} = \sqrt{\sigma_1^2}, \quad \varepsilon_{\text{eq,FF}} = \sqrt{\varepsilon_1^2} \quad (15)$$

Onset failure stress and strain (σ_{eq}^0 and $\varepsilon_{\text{eq}}^0$ in Eq. (13)) respectively correspond to the equivalent stress and strain when the value of damage function f_{IFF} or f_{FF} is equal to 1.0. The strain at final failure, $\varepsilon_{\text{eq}}^f$, is obtained by the damage evolution law, which prescribes equality between the dissipation of the strain energy density and the critical fracture energy density [at complete destruction](#). Based on the strain energy release density in material, a quadratic interaction function for the fracture energy in the mix-mode case can be established.

$$\left(\frac{g_2^k L^c}{G_{2c}^k}\right)^2 + \left(\frac{g_{21}}{G_{21c}^k}\right)^2 = 1 \quad (16)$$

where g_2^k and g_{21} are the strain energy release densities related to stresses σ_2 and τ_{21} , respectively. G_{2c}^k and G_{21c}^k are the critical fracture energy release rates respectively corresponding to transverse tension or compression and in-plane shear.

Among them, the superscript k ($k = t, c$) denotes the tensile or compressive load.

The strain energy density released in the characteristic length, due to complete IFF failure, g_i^f can be given in

$$g_i^f = \int_0^{\varepsilon_i^f} \sigma_i d\varepsilon \approx \frac{1}{2} \sigma_i^0 \varepsilon_i^f = \frac{1}{2} \sigma_i^0 \beta_i \varepsilon_{eq}^f \quad (i = 2, 21) \quad (17)$$

where σ_i^0 is the stress component at IFF failure initiation. β_i denotes the mix-mode ratio and can be expressed as

$$\beta_n = \frac{\langle \varepsilon_2 \rangle}{\varepsilon_{eq}}, \quad \beta_{21} = \frac{\gamma_{21}}{\varepsilon_{eq}} \quad (18)$$

The equivalent strain at final IFF failure is obtained by substituting the result of Eq. (17) into Eq. (16).

$$\varepsilon_{eq, IFF}^f = \frac{2}{L^c} \left[\left(\frac{\sigma_2^0 \beta_2}{G_{2c}^k} \right)^2 + \left(\frac{\tau_{21}^0 \beta_{21}}{G_{21c}^k} \right)^2 \right]^{-1/2} \quad (19)$$

The failure strain, at which FF failure across the element occurs, is determined by

$$\varepsilon_{eq, FF}^f = \frac{2g_{1c}^k}{R_{\parallel}^k} = \frac{2G_{1c}^k}{R_{\parallel}^k L^c} \quad (20)$$

where the superscript k ($k = t, c$) denotes the tensile or compressive load, G_{1c}^k is the critical fracture energy release rate of FF failure.

When an element is fully destructed, the localised stiffness reduction will result in an excessive element distortion that can cause convergence difficulties, slow-running or even abortion in numerical simulations. In order to resolve this numerical issue, the distortion controlling technique is used to help the fully destructed element maintain some residual stiffness, and all damage variables are limited to a maximum value of 0.99.

Thanks to identical fracture modes, the critical fracture energy release rates corresponding to transverse tension and in-plane shear (G_{2c}^t and G_{21c}) can be set equal to the values of mode I and mode II fracture energy release rates (G_{Ic} and G_{IIc}). The critical fracture energy release rate corresponding to transverse compression, G_{2c}^c , can be calculated approximately using the mode II component of the critical fracture energy release rates and the fracture angle ($G_{2c}^c \approx G_{21c} / \cos \alpha_0 = G_{21c} / \cos 53^\circ$ [19]). Thus, in this study, specific values of G_{2c}^t , G_{2c}^c , G_{21c} , G_{1c}^t and G_{1c}^c are $0.314 \text{ N}\cdot\text{mm}^{-1}$, $1.796 \text{ N}\cdot\text{mm}^{-1}$, $1.081 \text{ N}\cdot\text{mm}^{-1}$, $133 \text{ N}\cdot\text{mm}^{-1}$ and $10 \text{ N}\cdot\text{mm}^{-1}$ [14].

4.1.3. Surface-based cohesive contact approach for simulating delamination

A surface-based cohesive behaviour [20] is used to capture delamination by the application of generalised traction-separation behaviour for two adjacent surfaces. In contrast to cohesive interface elements, this in-built approach is typically convenient to simulate the cohesive connections without defining specific cohesive elements and tie constraints, and also allows simulation of other cohesive interactions, such as contact phenomena of adjacent surfaces.

The mix-mode failure is assumed to initiate when a quadratic interaction function involving the interface stress ratios reached a value of one. This quadratic stress criterion can be represented as

$$\left(\frac{\langle \tau_n \rangle}{\tau_n^0} \right)^2 + \left(\frac{\tau_s}{\tau_s^0} \right)^2 + \left(\frac{\tau_t}{\tau_t^0} \right)^2 = 1 \quad (21)$$

where τ_i ($i = n, s, t$) denote interfacial stresses in the normal, the first and the second shear directions respectively, and τ_i^0 are the corresponding strength associated with each direction.

The propagation of delamination was governed by B-K propagation criterion,

$$G_c = G_{Ic} + (G_{IIc} - G_{Ic}) \left(\frac{G_{II} + G_{III}}{G_I + G_{II} + G_{III}} \right)^\eta \quad (22)$$

where G_c is the mix-mode fracture energy rate, G_{Ic} and G_{IIc} are the critical energy release rate for mode I and mode II fracture respectively, G_I , G_{II} and G_{III} refer to the work done by the traction and its conjugate relative displacement in the normal, the first, and the second shear directions, respectively. And η is the mixed-mode interaction determined from experimental measurements.

The values of critical energy release rate for mode I fracture (G_{Ic}) and mode II fracture (G_{IIc}) can be measured by means of the double cantilever beam (DCB) test and edge notched flexure (ENF) test. The coefficient η can be measured by mix-mode bending (MMB) test. For the composite material IMS/X850, specific values of G_{Ic} , G_{IIc} and η are $0.314 \text{ N}\cdot\text{mm}^{-1}$, $1.081 \text{ N}\cdot\text{mm}^{-1}$ and 2, respectively. The interfacial strength, which is used to evaluate the onset of delamination, might not be obtained by tests directly. Because in the short beam shear test, the interlaminar shear strength is evaluated by the ultimate failure load rather than the shear stress characterizing the onset of delamination. A micromechanical model based on equivalent inclusion method (EIM) has been used to predict the initial interlaminar strength [21]. The cohesive strength based on the assumption that delamination initiates due to matrix cracking, can be taken as the value of the interface stress when the maximum of the matrix stress in the cohesive layer initially reaches to the matrix strength. Thus, the corresponding interfacial strength, which can be determined by a micromechanical model based on

EIM, is: $\tau_n^0 = 51\text{MPa}$; $\tau_s^0 = \tau_t^0 = 62\text{MPa}$ for the composite material IMS/X850. In addition, an engineering approach, proposed by Turon et al. [22], is used to still accurately capture the softening behaviour ahead of the crack tip with a relatively coarser mesh size, through reducing interface strength. Thus, in this context, the values of τ_n^0 , τ_s^0 and τ_t^0 are 25.5 MPa, 31MPa and 31 MPa respectively, yielding a fine mesh size of 2 mm.

4.2. Global-local modelling technique

The Abaqus/Explicit code is used to run the numerical FE model where the composite damage model, together with the proposed simplified method for incorporating impact damage, is implemented. The composite panels are discretized using the continuum shell elements (SC8R). In order to reduce high computational costs, a "Global-local technique" has been applied. In general, a refined mesh (2 mm element average size) has been adopted in the central part including the edge impacted stiffener and corresponding skin (local region), while a coarser mesh (5 mm element average size) has been considered in the rest of model (global region), as shown in Fig. 6(a).

"Tie-constraints" contact algorithms have been used to link the node degrees of freedom at the global-local boundaries.

The boundary conditions, namely the "anti-buckling knife edges" and the "bottom clamping blocks", are simply simulated by constraining the corresponding degree of freedom of the nodes. A longitudinal compression displacement, relatively to the loading conditions, has been applied at the lower end of the stiffened panel. Although the loading rate in the CAEI test is controlled with 0.02 mm/s to obtain the ideal

quasi-static test condition, it is simulated by means of the dynamic FE code ABAQUS/Explicit to overcome severe convergence difficulties encountered with implicit analysis when modeling issues with high material and geometrical non-linearity. Accordingly, the loading rate is chosen at 3.75 m/min to reduce CPU time [14]. As matter of the fact, results from a quasi-static analysis at such loading rate can be considered good, due to the fact that the ratio of kinetic energy to internal energy does not get too large (generally less than 10%).

In the FE model of CAEI, surface-based cohesive contact is only inserted at representative interfaces with the highest risk of delamination. Considering the stress concentration caused by stiffness mismatch of adjacent plies, these representative interfaces are selected as (see in Fig. 6(b)): (1) the interfaces at the boundaries of the crushing zone (the 1st and 4th interfaces), (2) the interfaces at the maximum delamination induced by edge impact (the 2nd and 3rd interfaces), (3) the distinct interface generating in the manufacturing process of the bottom flange (the 5th interface) and (4) the co-cured interfaces between the bottom flanges of stiffeners and corresponding skin (the 6th interface). A “general contact” algorithm has been applied between the plies, in order to avoid unrealistic penetration.

5. Experimental verification

5.1. Numerical and experimental comparison of failure load

For three configurations of stiffened panels, the applied load versus global strain curves predicted by the FE models (denoted by 'Num'), are compared with the experimental results (denoted by 'Exp'), as shown in Fig. 7. To be noted, the global

strain, which is along the direction of compression load, can be calculated through dividing the corresponding displacement at the loading end by the effective length (560 mm) of the panel. Before impact at the edge of the stiffener web, the axial compression load is first imposed and incremented in steps up to the strain level of about $3500 \mu\epsilon$. It has been verified, by nondestructive testing, that damage does not occur for all pristine panels under above strain level. Thus, even though the destruction of the pristine stiffened panel in compression is not investigated and the ultimate failure load is not measured, the initial stiffness of the pristine specimen can still be obtained by the slope of the load-strain curve (see Fig. 7). The predicted curves in Fig. 7 correlate well with the measured results recorded for CAEI experiments of the stiffened composite panels A-T, B-T and C-T. The initial stiffness of the specimens, ultimate failure load and strain are all captured with good accuracy. The predicted maximum loads of 1346 kN, 1906 kN and 2762 kN, corresponding to Panels A-T, B-T and C-T, closely match the average measured values of 1425 kN, 1783 kN and 2524 kN. The ultimate failure loads are predicted to within 10% of average measured results (the error of -5.5%, 6.9% and 9.4% for Panels A-T, B-T and C-T, respectively).

In general, the CAEI process of the specimen can be divided into two phases: the complete fracture of the impacted web and the catastrophic failure of the whole stiffened panel. Fig. 8 makes comparisons between measured and predicted results for the compressive strength corresponding to above two failure phases of stiffened composite panels, where the compressive strength is defined as the ratio of the compressive load to the cross-sectional area of stiffened panels. Compared to average

values of experimental data, we note that the error of -5.5%, 2.8% and -2.7%, for Panels A-T, B-T and C-T respectively, appears in the simulation of compressive strength corresponding to complete fracture of the impacted web (denoted by σ_{web}), and the error increases to -5.5%, 6.9% and 9.4% for compressive strength corresponding to the ultimate failure of the whole stiffened panel (denoted by σ_{panel}). Clearly, the compressive strength (or the failure load) at both two failure phases can be predicted to within 10% of average measured results. In fact, although the experimental results exhibit relatively high dispersion (especially for Panels C-T), all predicted points are within the data bands formed by measured points for all test configurations. So above all, it confirms an acceptable level of correlation achieved between experiment and numerical analysis in terms of the failure load and compressive strength, which validates the failure formulation proposed.

It can be clearly known, from the load-global strain curves recorded during CAEI experiments (Fig. 7), that for most of the T-stiffened composite panels induced by edge impact damage, the slopes of their curves always keep linear until the compression load is increased to the ultimate failure load. There isn't any nonlinear change in stiffness of test panels, except for specimens B-T-01 and C-T-02 (see Figs. 7(b) and 7(c)), can be clearly observed in the entire loading process, and the ultimate collapse of panels occurs in a flash, accompanied with a dramatic load drop and a harsh noise during CAEI experiments. This phenomenon implies that, once the complete fracture of the impacted stiffener occurs, in the most case of our stiffened panels, the residual structure including the skin and other two stiffeners is not able to bear the additional load instantaneously

transferred from the broken stiffener. By comparing to experimental results of pristine panels, the stiffness reduction caused by edge impact damage is minimal, which is to be expected as the extent of impact-induced damage is not extensive for the whole stiffened panel.

In the numerical simulation, as illustrated in Fig. 9, there is a distinct knee point in each load-global strain curve, regardless of specimen configurations. This knee point is found to be related with complete fracture of the impacted web, and can be considered as a transition point of the CAEI failure process: the complete fracture of the impacted web and the catastrophic failure of the remaining intact structure. During the initial compression load (prior to the knee point), the load-strain curve of impacted stiffened panel coincides perfectly with that of the pristine panel, which achieves good correlation with CAEI data (see Fig. 7). However, after the knee point, instead of a sudden load drop that can be observed in most measured curves of CAEI specimens, the predicted load can still reach or even exceed the load corresponding to the destruction of the impacted web. These knee points found in the numerical analysis are only able to be validated by the evolutions of measured curves of specimens B-T-01 and C-T-02 in the CAEI experiments. It can be inferred that a knee point will appear in the load-global strain curve if the remaining intact structure is able to bear the additional load instantaneously transferring from the broken stiffener, otherwise the panels will collapse in an instant, accompanied with a sudden load drop, and therefore no knee point could be found in the curve. Furthermore, as illustrated in Fig. 8, it can be noticed that once fracture of the impacted web occurs, in contrast to the instantaneous collapse observed

in experiment ($\sigma_{\text{web}} = \sigma_{\text{panel}}$), the remaining intact structure can still sustain the additional load transferred from the broken stiffener ($\sigma_{\text{web}} \leq \sigma_{\text{panel}}$) in simulation. That is, the main reason given for the appearance of a knee point in the numerical load-strain curve is attributed to overestimating the compressive strength of the remaining intact structure after fracture of the impacted web. When the impacted web is destroyed by the compression, additional out-of-plane bending moment will be formed at the central section of the stiffened composite panel, due to severe loss in structural stiffness at the rupture position. Afterwards, accompanied with sudden increment of the out-of-plane deformation, significant interlaminar stresses (σ_3 , τ_{13} and τ_{23}) will be generated at the bottom flange and skin near the rupture position of the impacted web. For the stiffened composite panel designed with thick skin or bottom flange (i.e. Panel C-T, of which the thickness of the skin and bottom flange is 7.77 mm and 4.44 mm respectively), in particular, the effect of interlaminar stresses on the matrix failure should not be neglected. Meanwhile, delamination at the interfaces between adjacent plies may be a major failure mode that can promote the ultimate failure of the stiffened composite panel (see in Fig. 10). However, as Puck's failure criteria used to judge the presence of fiber and matrix damage is established according to the plane stress condition, the influence of interlaminar stresses on the matrix damage can not be taken into account in the present FE mode. Besides, limited by the computation scale, the interfacial failure, namely delamination, which might occur between adjacent plies, is not simulated for the bottom flange and skin subjected to the additional out-of-plane bending moment. Therefore, the accuracy of the model in predicting ultimate failure load may be affected,

especially for Panel C-T with thick skin and bottom flanges, which will result in a slowly rising process in the predicted load-global strain curve after the fracture of impacted web (see evolutions after knee points in Fig. 9). In summary, it can be noticed that the model has demonstrated a predictive capability in the load corresponding to the fracture of impacted web, but seems to predict the ultimate failure load in an inadequate way because of overestimating the load-carrying capacity of the remaining intact structure. In order to correct this problem it seems necessary to consider the effect of interlaminar stresses in the failure analysis of the remaining intact structure in compression.

In addition, the load-carrying capacity of pristine panels is included for comparison, as shown in Fig. 9. Compared with the response of pristine panels, numerical results exhibit that the residual strength of damaged panels reduces to 71.9%, 65.7% and 57.6% of the pristine stiffened panels, for Panels A-T, B-T and C-T respectively. It obviously demonstrates the free edge impact vulnerability of stiffened panels.

5.2. Ultimate failure modes

Fig. 11 gives the ultimate failure modes and locations of the T-stiffened panel subjected to compression after edge impact (taking an example of specimen B-T), both experimentally and numerically. During the CAEI experiments, the skin and the stiffener subjected to edge impact, obviously fail along the horizontal line at the middle height of the specimen (see in Fig. 11(a)). The predicted intralaminar damage maps of Figs. 11(b) and 11(c) suggest that the damages like matrix cracking and fiber breakage have developed severely inside the plies at the middle of the specimen, which means

thoroughgoing fracture in practice. By comparison with matrix failure, fiber breakage, basically characterized by a single dominant crack, is observed to initiate from the central edge impacted location and then propagate towards two side edges of the stiffened composite panel. It indicates that the propagation of fiber compressive failure plays a more significant role than matrix failure in the compressive mechanism piloting the residual strength after edge impact.

Additionally, the phenomenon of out-of-plane swelling, as a specific behaviour from the CAEI tests, is successfully captured at the edge impact location. Obvious local debonding at interfaces between all the three bottom flanges and corresponding skin areas is detected near the fractured middle locations of panels, which is consistent with experimental observations. Above all, the numerical results exhibit a good correlation with experimental results.

5.3. Strain distribution

Due to the similarity of damage scenario for all specimens, in this section only Panel B-T is presented as an illustrative example. The predicted strain-load curves of representative strain gauges are given in [Fig. 12](#), which provide a good quantitative match with the experimental observations. The specific number and location of selected strain gauges can be found in [Fig. 2\(b\)](#). The local buckling load, which can be identified by the strain divergence of back-to-back strain gauges located on the center of the impacted web (G93 and G94), is about 1627 kN in the numerical simulation, as shown in [Fig. 12\(a\)](#). In CAEI experiments, for the four specimens of the B-T configuration, the measured buckling loads, which can be identified by clear divergence in strains between

pairs of back-to-back gauges, are about 1400 kN, 1600 kN, 1690 kN and 1890 kN.

Compared with the average one measured in experiments (about 1645kN), the error is 1.1% for Panel B-T. Moreover, with consideration for the experimental dispersion, the predicted buckling load of 1627 kN is still within the interval (from 1400 kN to 1890 kN) formed by the measured data. When the applied load reaches 1809 kN, the impacted web breaks off in the middle. As a matter of fact, the complete failure of the impacted web severely weakens the support for the surrounding skin and bottom flange, and the strain distribution in exterior and interior areas will be affected by a change in the buckling shape. It can be seen from Figs. 12(b) and 12(c) that the strain-load curves for back-to-back strain gauges on the central skin (G9 and G10) and those on the flange of the impacted stiffener (G47 and G50) do not maintain linear, and their slopes begin to change in two opposite direction. At the same time, as a result of load redistribution, additional load will transfer from the broken web to the remaining structure, particularly to other two intact stiffeners. Therefore, it will cause an increase of compression strain in undamaged stiffeners (see in Fig. 12(d)).

6. Discussions

One of the main objectives of this investigation is to study the compressive failure mechanisms of the T-stiffened composite panel subjected to compression after edge impact. As mentioned previously, it has been realized that two distinct CAEI phases, which can be identified by careful inspection into the distinct knee point of the load-global strain curve, are the complete fracture of the impacted web and the catastrophic failure of the whole stiffened panel. However, the compressive failure

mechanisms of the edge impacted web is still required to be investigated in detail, in order to acquire how the edge impact-induced damage affects the CAEI failure mechanism during the loading phase. In particular, due to the fact that the phenomena of out-of-plane swelling and fiber breaking are always observed in the final failure of different specimen configurations, it might suggest that local buckling and the compressive fiber failure are two critical factors in reducing the structural strength. The failure mode that most affects the residual strength should be concluded, even if the interaction between these two phenomena seems to be in a complicated way.

The initiation and subsequent propagation of both fiber compressive fracture and delamination are discussed. Note that when the scalar stiffness degradation for cohesive surfaces (CSDMG) reaches the value one, it represents complete stiffness degradation and hence delamination. Meanwhile once the value of [the No.11 solution dependent state variable \(SDV11\)](#), which stores the computation result of the fiber compression fracture criterion (Eq. (9)), equals to one, the fiber compressive failure initiates. Considering the similarity for all specimens, in this section only specimen B-T is taken into consideration.

In order to illustrate the CAEI damage scenario of the impacted web clearly, all ply groups and interfaces are numbered according to their relative geometric positions from left to right, as shown in [Fig. 6\(b\)](#). In the initial phase of compression, when the compression load reaches 1627 kN, the local buckling of the impact region with semi-elliptical delamination can be clearly distinguished by the strain divergence of strain gauges G93 and G94 ([Fig. 12\(a\)](#)), but neither fiber failure nor interfacial

delamination is detected in the numerical simulation, as illustrated in Figs. 13(a) and 14(a). Afterwards, at the applied load of approximately 1733 kN, fiber compressive failure firstly occurs at the 1st ply group (Fig. 13(b)), which is attributed to the high stress concentration caused by the loss of stability of the sublaminae (the 1st and 2nd ply groups) and the stiffness mutation at the boundaries of the crushing zone (the 1st interface). It can be inferred that the local buckling of the sublaminate, which can be separated by the maximum delamination induced by edge impact, is probably the trigger of the damage formation in CAEI. As the compressive load increases, accompanied with aggravating out-of-plane deformation, delamination at the 1st interface is observed successively around the boundaries of the crushing-related zone. Meanwhile, fiber compressive failure propagates rapidly along the transverse width perpendicular to the applied load, from the crushing zone belonging to the 2nd and 3rd ply groups. After the load reaches around 1759 kN, both intralaminar and interlaminar damage occurs at the position where the strain gauge G93 is bonded, as shown in Figs. 13(c) and 14(c). As a result of the significant stiffness reduction caused by failure, it will lead to a sudden change of the strain-load curve for strain gauge G93. Eventually, due to the fact that most ply groups are heavily damaged, the impacted web collapses at the fracture load of 1809 kN (Figs. 13(d) and 14(d)).

In general, concerning the influence of the impact-induced damage on the CAI failure mechanisms, contrary to the case of classic skin impact where the fiber compressive failure adjacent to the impactor or wedge effect is not always evident, the crushing-related failure, such as the fiber compressive failure and wedge debris, plays a

major role on the failure mechanisms that drive the residual strength after edge impact.

Due to entire loss of the load-carrying capacity in the crushing zone, severe stress concentration, caused by stiffness mutation, will result in the initiation of fiber compressive failure near the boundary of the crushing zone. The free edge of a T-stiffener web, subjected to low-velocity impact, might induce a crushing zone severely damaged under the impactor, which includes fiber compressive failure that would propagate during the CAEI and eventually lead to the complete fracture of the impacted web.

In other words, compression failure of composite structures previously damaged by impact loading is due to the propagation of impact-induced damage. It suggests that the physical controlling mechanisms during an impact event should be considered, in order to achieve a reasonable composite method for the prediction of the residual strength. From an experimental point of view, the analysis of structural responses and damage modes, in a suite of dynamic and quasi-static tests about edge impact, gives interesting information on the specific failure mechanisms which indicate that failure behaviour of the composite structure subjected to edge impact exhibits similarities with the one in progressive crushing tests, rather than classic skin impact. So a simplified approach, which makes an analogy between the damage at the impacted region to that of composite crushing, is presented in this study to estimate geometrical dimensions of edge impact-induced damage. The maximum depth of crushing zone, which can hardly be measured by current testing techniques, is obtained according to unique phenomena occurring during the edge impact process, such as a relatively constant force plateau in

the force-displacement curve and approximate equivalence between the global mean crushing stress and transverse compressive strength. It should be emphasized that the mean crushing stress, summarized from experiment phenomena, is not a material characteristic, but a structure dependent value at a macro scale. In fact, at a micro or mesoscopic scale, a constant force plateau does not imply a constant stress because the contact area is changing as the impact progresses.

Besides, the value of coefficient k , corresponding to the maximum compressive deformation of the damaged material point, is a starting point and is little more than a guess consistent with some published results. Indeed, this evaluation process limits the predictive character of this model. Consequently, it requires a detailed finite element model through combination of fracture mechanisms, which involve fiber fracture, matrix cracking, fiber-matrix debonding, fiber kinking and delamination at a micro or mesoscopic scale, based on a large number of experimental investigations on the T-stiffener subjected to edge impact loading. But to the author's knowledge, the experimental and numerical study concerning the failure mechanisms of the laminates subjected to edge impact is limited in the literature. Hence, the coefficient k in Eq. (4) is set equal to 0.3 using an inverse approach: this value is varied to find a best match between measured and predicted failure loads for one CAEI case (i.e. Panel B-T). It is necessary to carry out a sensitivity study of the coefficient k using in the simplified model of edge impact damage, and in particular to evaluate the accuracy of the determined value of 0.3. With the CAEI model of Panel B-T, five values are compared in a reasonable interval from 0.083 (through dividing the maximum displacement of the

impactor by the minor axis of semi-elliptical delamination) to 1.0. The load-strain curves and failure loads at different failure phases obviously make possible to identify the influence of the coefficient k , as illustrated in Fig. 15. It is seen that the coefficient k particularly plays a significant role in the results predicted by the CAEI modelling, including the fracture load of the impacted web (identified by the knee points of curves in Fig. 15(a)) and the ultimate failure load of the stiffened panel (identified by the maximum values of curves in Fig. 15(a)). When the coefficient is less than the value of 0.45, with the increase in the coefficient k , the fracture load of the impacted web increases but the ultimate failure load of the stiffened panel remains approximately constant (see Fig. 15(b)). Instead, when k exceeds the value of 0.45, both two failure loads increase with increasing this coefficient, and values of these loads are nearly equal. Mainly it is because when the value of k is lower, which means the maximum depth of the crushing zone may be larger than the actual size, the load-carrying capacity of the impacted web may be underestimated in the numerical simulation. However, as the impacted web is broken at a relatively low load level, the ultimate failure load is determined by the remaining intact structure, and thus it keeps basically unchanged (i.e. $k = 0.083, 0.15$ and 0.3). In contrast, if the value of k is higher than a certain value (i.e. 0.45), the complete fracture of the impacted web is predicted to occur at a high load level, which means the remaining intact structure may have no ability to sustain this high load. In this case, the ultimate failure load of the stiffened panel is nearly equal to the fracture load of the impacted web. From Fig. 15(b), it seems plausible to observe the numerical results with a coefficient of 0.3 present the best correlation with the mean

values obtained by experimental measurement, in terms of failure loads corresponding to the fracture of the impacted web and the ultimate failure of the stiffened composite panel.

Of course this study is only preliminary work and some hypotheses should still be detailed and confirmed further. However, with a range of three different specimen configurations, the results presented in this paper demonstrate the predictive capabilities of the proposed method, in terms of both quantitative (failure load, residual strength and load-strain curves) and qualitative (extent and shape of different damage modes) aspects. Meanwhile, the CAEI failure mechanisms of the T-stiffened composite panel, to some extent, are able to be revealed.

7. Concluding remarks

A computationally efficient, phenomenological-based mechanical model has been formulated and implemented into the FE code ABAQUS/Explicit. The numerical results have been validated against experimental results, and the proposed model has shown a high degree of capability in predicting the residual strength and elaborating the CAEI failure mechanisms. The following conclusions can be drawn from the present study.

In contrast to the classic skin impact, the presence of crushing zone after edge impact is regarded as a key consideration in the CAEI failure prediction of the T-stiffened composite panel. With the help of a composite damage model comprising the CDM model and surface-based cohesive contact model, rather good correlation has been obtained between experimental and numerical results, in terms of failure load, residual strength, load-strain curves and typical failure morphology of the stiffened

composite panels subject to compression after edge impact.

The fiber compressive failure, which initiates near the boundaries of the impact-induced crushing zone and propagates along a horizontal line perpendicular to the compression loading direction, plays a major role in the fracture of the T-stiffener subjected to edge impact.

Regardless of configurations of stiffened composite panels, the complete fracture of the web with damage induced by free edge impact can seriously affect or even control the failure mechanisms and the residual strength of the stiffened panels.

Rationally, the extent of the influence depends on the ratio of the stiffener to the skin.

Compared to both experimental and numerical results of pristine panels, the stiffness reduction caused by edge impact damage is minimal before the complete fracture of the edge impacted web, which is to be expected as the extent of impact-induced damage is not extensive for the whole stiffened panel in this study. It can be noticed that the CAEI failure of the stiffened composite panel contains two phases of the complete fracture of the impacted web and the catastrophic failure of the remaining intact structure. In order to avoid overestimating the load-carrying capacity of the remaining intact structure, the influence of interlaminar stresses on composite material damages is recommended to be taken into account.

To be concluded, this investigation establishes a specific numerical approach to predict the residual strength and elaborate the CAEI failure mechanism. By this way, it will be effective to evaluate the damage tolerance of stiffened composite panels under edge impact and meanwhile improve CAEI relevant knowledge.

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References

- [1] Soutis C, Curtis PT. Prediction of the post-impact compressive strength of CFRP laminated composites. *Compos Sci Technol* 1996; 56: 677–84.
- [2] Li N, Chen PH. Experimental investigation on edge impact damage and Compression-After-Impact (CAI) behavior of stiffened composite panels. *Composite Structures* 2016; 138: 134-150.
- [3] Fardin E, Christos K. An efficient approach to determine compression after impact strength of quasi-isotropic composite laminates. *Composites Science and Technology* 2014; 98: 28-35.
- [4] Andrew T R, Richard B. Compressive static strength model for impact damaged laminates. *Composites Science and Technology* 2009; 69: 2301-2307.
- [5] Rivallant S, Bouvet C, Abi Abdallah E, et al. Experimental analysis of CFRP laminates subjected to compression after impact: the role of impact-induced cracks in failure. *Composite Structures* 2014; 111: 147-157.
- [6] Chen P H, Shen Z, Wang J Y. Strength prediction of notched composite laminate. *Composite Science and Technology* 2001; 61(9):1311-1321.
- [7] Chen P H, Shen Z, Wang J Y. A new method for compression after impact strength prediction of composite laminates. *Journal of Composite Materials* 2002; 36(5): 589-610.

- [8] Malhotra A, Guild F J. Impact damage to composite laminates: effect of impact location. *Appl Compos Mater* 2014; 21: 165-177.
- [9] Rhead A T, Marchant D, Butler R. Compressive strength of composite laminates following free edge impact. *Composites: Part A* 2010; 41: 1056-1065.
- [10] Ostre B, Bouvet C, Lachaud F, et al. Edge impact damage scenario on stiffened composite structure. *Journal of Composite Materials* 2015; 49:1599-1612.
- [11] Ostre B, Bouvet C, Minot C, et al. Experimental analysis of CFRP laminates subjected to compression after edge impact. *Composite Structures* 2016; 152: 767-778.
- [12] Ostre B, Bouvet C, Minot C, et al. Finite element analysis of CFRP laminates subjected to compression after edge impact. *Composite Structures* 2016; 153: 478-489.
- [13] Ostre B, Bouvet C, Lachaud F, et al. Edge impact modeling on stiffened composite structures. *Composite Structures* 2015; 126: 314-328.
- [14] Tan W, Falzon BG, Chiu LN, et al. Predicting low velocity impact damage and Compression-After-Impact (CAI) behaviour of composite laminates. *Composites: Part A* 2015; 71: 212–236.
- [15] Li N, Chen PH. Micro–macro FE modeling of damage evolution in laminated composite plates subjected to low velocity impact. *Composite Structures* 2016; 147: 111-121.
- [16] Israr H A, Rivallant S, Bouvet C, et al. Experimental investigation on mean crushing stress characterization of carbon-epoxy plies under compressive crushing

- mode. *Composite Structures*, 2013, 96: 357–364.
- [17] Israr H A, Rivallant S, Bouvet C, et al. Finite element simulation of 0/90 CFRP laminated plates subjected to crushing using a free-face-crushing concept. *Composites: Part A*, 2014, 62: 16-25.
- [18] Hwang SF, Liu GH. Buckling behaviour of composite laminates with multiple delaminations under uniaxial compression. *Composite Structures* 2001; 53: 23-43.
- [19] Maimi P, Camanho P P, Mayugo J A, et al. A continuum damage model for composite laminates: Part II - Computational implementation and validation. *Mechanics of Materials*, 2007; 39(10): 909-919.
- [20] ABAQUS. ABAQUS 6.10 Documentation. [Dassault systems](#); 2010.
- [21] Ye Q, Chen PH. Prediction of the cohesive strength for numerically simulating composite delamination via CZM-based FEM, *Composites Part B* 2011; 42: 1076-1083.
- [22] Turon A, Davila CG, Camanho PP, Costa J. An engineering solution for mesh size effects in the simulation of delamination using cohesive zone models. *Eng Fract Mech* 2007; 74(10):1665–82.
- [23] [Chinese Aeronautical Establishment](#). [Handbook of composite structure design](#). Beijing: Aviation Industry Press; 2001. (in Chinese)
- [24] [US Department of Defense](#). [MIL-HDBK-17/3F](#), Department of defense handbook: polymer matrix composites-materials usage, design and analysis. USA: Department of Defense; 2002.
- [25] [Yan H](#), [Oskay C](#), [Krishnan A](#), [Xu L R](#). Compression-after-impact response of

woven fiber-reinforced composites. *Composite Science and Technology* 2010;
70:2128-2136.

[26] Knops M. Analysis of failure in fiber polymer laminates: the theory of Alfred Puck.
Germany: Springer; 2008.

[27] Deuschle HM. 3D failure analysis of UD fibre reinforced composites: Puck's theory
within FEA. Germany: University Stuttgart; 2010.

[28] Jones RM. Mechanics of composite materials: second edition. Virginia: Taylor &
Francis; 1999.

[29] Bazant ZP, Oh BH. Crack band theory for fracture of concrete. *Mater Struct* 1983;
16:155-77.

Fig. 1. (a) Isometric view of T-stiffened panel, (b) stiffened composite panel nominal dimensions (scale in mm) and (c) cross section of the stiffener and skin.

Fig. 2. (a) Stiffened panel installation on the hydraulic compression test machine and (b) locations of strain gauges.

Fig. 3. (a) Post-impact visual inspection and C-scan image of the free edge impact location and (b) typical force-displacement response of the edge impact experiment.

Fig. 4. Schematic of typical edge impact induced damage morphology.

Fig. 5. (a) Details in the numerical CAEI model of a stiffened composite panel and (b) schematic of the web subjected by edge impact damage

Fig. 6. Comparison between experimental and numerical load-global strain curves of stiffened composite panels

Fig. 7. Comparison between experimental and numerical compressive strength corresponding to: (a) the complete fracture of impacted webs and (b) the ultimate failure of stiffened composite panels.

Fig. 8. Failure morphology of the stiffened composite panel: (a) representative failure modes in the CAEI test, (b) fiber compressive failure and (c) matrix compressive failure.

Fig. 9. Applied compressive load as a function of the measured strains in: (a) edge impacted web, (b) skin, (c) flange of the impacted stiffener, and (d) web of undamaged stiffener.

Fig. 9 (continued)

Fig. 10. Propagation of fiber compressive failure in the edge impacted stiffener: applied

load is equal to the value of (a) 1627 kN, (b) 1733 kN, (c) 1759 kN and (d) 1784 kN.

Fig. 11. Propagation of delamination in the edge impacted stiffener: applied load is equal to the value of (a) 1627 kN, (b) 1733 kN, (c) 1759 kN and (d) 1784 kN.

Table 1 Unidirectional lamina material proprieties

IMS/X850 UD properties		
E_1	Longitudinal Young modulus	165 GPa
E_2	Transverse Young modulus	8.58 GPa
G_{12}	In-plane shear modulus	4.57 GPa
ν_{12}	Poisson's ratio	0.331
$R_{\square}^t$	Longitudinal tensile strength	3071 MPa
$R_{\square}^c$	Longitudinal compressive strength	1747 MPa
$R_{\perp}^t$	Transverse tensile strength	88 MPa
$R_{\perp}^c$	Transverse compressive strength	271 MPa
$R_{\perp\square}$	In-plane shear strength	143 MPa
E_{f1}	Young modulus for fibers	225 GPa
ν_{f12}	Poisson's ratio for fibers	0.2
G_{Ic}	Critical ERR-Mode I	0.314 N mm^{-1}
G_{IIc}	Critical ERR-Mode II	1.081 N mm^{-1}
t_p	Ply thickness	0.185 mm

Table 2 Geometric parameters for modeling edge impact damage

		A-T	B-T	C-T
E	Edge impact energy level /J	32.2	27.3	39.7
l	Major axis of semi-elliptical delamination /mm	100	95	136
w	Minor axis of semi-elliptical delamination /mm	36	30	45
b_{del}	Position of maximum delamination /mm	2.775	2.035	4.07
a	Length of the crushing damage region /mm	10	10	12
b	Width of the crushing damage region /mm	5.2	5.2	5.9
D_{crush}^a	Depth of the crushing damage region /mm	9.7	8.3	8.8

^a indicates the estimated value in Eq. (4)