

Predicting Impact Damage, Residual Strength and Crashworthiness of Composite Structures

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ABSTRACT

The development of the latest generation of wide-body carbon-fibre composite passenger aircraft has heralded a new era in the utilisation of these materials. The premise of superior specific strength and stiffness, corrosion and fatigue resistance, is tempered by high development costs, slow production rates and lengthy and expensive certification programmes. Substantial effort is currently being directed towards the development of new modelling and simulation tools, at all levels of the development cycle, to mitigate these shortcomings. One of the primary challenges is to reduce the extent of physical testing, in the certification process, by adopting a 'certification by simulation' approach. In essence, this aspirational objective requires the ability to reliably predict the evolution and progression of damage in composites. The aerospace industry has been at the forefront of developing advanced composites modelling tools. As the automotive industry transitions towards the increased use of composites in mass-produced vehicles, similar challenges in the modelling of composites will need to be addressed, particularly in the reliable prediction of crashworthiness. While thermoset composites have dominated the aerospace industry, thermoplastics composites are likely to emerge as the preferred solution for meeting the high-volume production demands of passenger road vehicles. This keynote paper outlines recent progress and current challenges in the development of finite-element-based predictive modelling tools for capturing impact damage, residual strength and energy absorption capacity of thermoset and thermoplastic composites for crashworthiness assessments.

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INTRODUCTION

Composite materials are finding increasing utilisation in a number of transportation industries concerned with making structures lighter to reduce environmental impact and improve efficiency. Nevertheless, composite structures are susceptible to damage from low-velocity impact events (e.g. accidental damage incurred in service or during routine maintenance). Even with barely visible impact damage, CAI residual strength can be significantly reduced. Another major challenge in the development of land-based mass-transportation fibre-reinforced polymer composite vehicles is ensuring a prescribed level of crashworthiness [1]. While the potential superior energy absorbing capacity of carbon-fibre composite structures is repeatedly demonstrated in Formula One racing [2], the design of a cost-effective crashworthy carbon-fibre reinforced polymer automotive passenger cabin has yet to be realised.

One approach for reducing development time and cost of composite structures is to increase the use of modelling and simulation at all levels of the product development cycle. The aerospace and automotive industries have been at the forefront of integrating modelling and simulation tools into their product development [3], but the shift towards the increased use of composite materials has highlighted the inadequacy of existing simulation tools to reliably predict the structural response of composites under damage-inducing loads. The exhaustive experimental programme, currently required as

part of the development and certification of a new composite airframe is testament to this lack of capability. Current practice follows a building block approach which was first represented by Rouchon [4] as a test pyramid. The purpose of adopting this approach is to mitigate risk by progressing through a sequence of testing; starting from simple (generic) coupons, for basic material characterisation, and moving to more complex (non-generic) structural details as shown in Figure 1. This is costly and highly inefficient. The need for replacing some of this physical testing by simulation, as suggested in Figure 1, is equally applicable to the development of automotive composite structures where crashworthiness assessments are critical, placing additional demands on computational requirements.

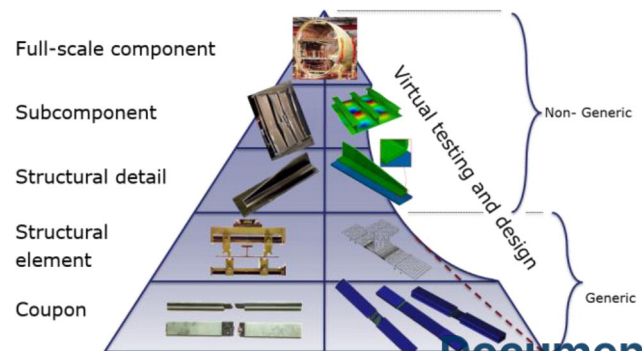


Figure 1. Rouchon pyramid for certification of composite aerostructures.

This paper will focus on recent developments in addressing the challenge of developing a mesoscale finite element based damage model, which builds on the work of Falzon's research group [5,6,7,8,9], for predicting impact damage, residual strength and energy absorption capacity of carbon fibre composites structures.

1. MESOSCALE DAMAGE MODEL

The failure modes exhibited by laminated composites may be classified as intralaminar (matrix cracks and fibre pullout/breakage) and interlaminar (delamination) damage as shown in Figure 2.

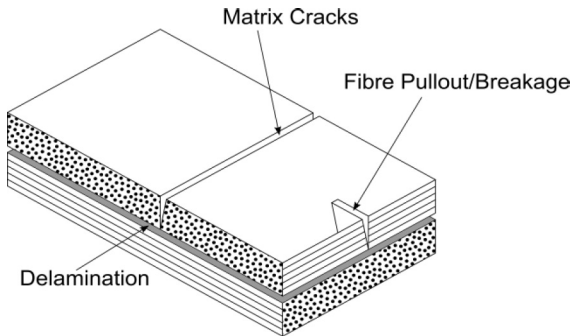


Figure 2. Damage modes in laminated composites.

1.1. Interlaminar Damage Model

The surface-based cohesive behaviour in ABAQUS/Explicit [10] was used to capture delamination using a bilinear traction-separation relationship. This approach is a convenient means to model the cohesive connections without the need to define cohesive elements and tie constraints. Failure initiation was governed by a quadratic stress criterion,

$$\left(\frac{\tau_1}{\tau_1^0}\right)^2 + \left(\frac{\tau_2}{\tau_2^0}\right)^2 + \left(\frac{\tau_3}{\tau_3^0}\right)^2 \leq 1, \quad (1)$$

where τ_i ($i = 1, 2, 3$) are the stresses in the in-plane directions (1,2) and normal direction (3) respectively, and τ_i^0 are the corresponding maximum stresses associated with each direction, $\tau_{sh}^0 = \sqrt{(\tau_1^0)^2 + (\tau_2^0)^2}$. Delamination was propagated using a mixed-mode relationship proposed by Benzeggagh and Kenane (B-K propagation criterion) [11],

$$G_c = G_{Ic} + (G_{IIc} - G_{Ic})B^\eta, \quad (2)$$

where G_c is the mixed-mode fracture toughness, B is the local mixed-mode ratio defined as $B = G_{shear}/(G_I + G_{shear})$. As mode III is not considered, $G_{shear} = G_{III}$. η is the mixed-mode interaction determined from experimental measurements. The mixed-mode softening law is shown in Figure 3.

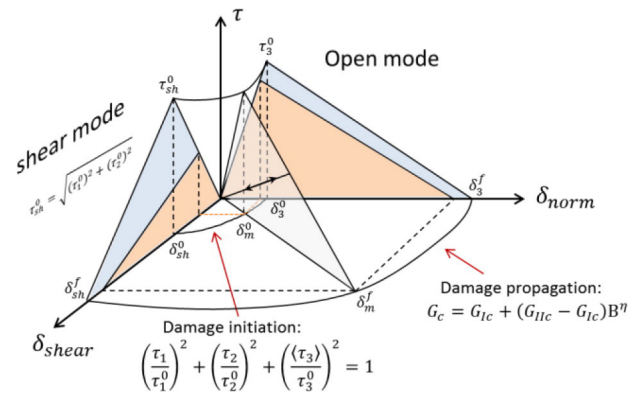


Figure 3. Mixed-mode softening law [12].

1.2. Intralaminar Damage Model

The developed intralaminar damage model is based on a continuum damage mechanics approach proposed by Lemaitre and Chaboche [13], as a method to determine the behaviour of a material under damage-inducing loads. The effective stresses are defined as stresses transmitted across the intact part of the cross-section in a Representative Volume Element (RVE). The damage tensor is a function of three monotonically increasing damage variables, bound by 0 (no damage) and 1 (complete failure), each relating to a form of damage mode under a different loading state; (i) d_{11}^T refers to tensile damage in the fibre direction, (ii) d_{11}^C refers to compressive damage in the fibre direction and (iii) d_{mat} refers to matrix cracking due to a combination of transverse tension/compression and shear loading, which is a unified matrix damage mechanism. The components of the effective stress tensor, $\tilde{\sigma}$, and true stress tensor, σ , can be linked by the damage tensor, D , undamaged material elasticity tensor, C , and the strain tensor ϵ ,

$$\sigma = D\tilde{\sigma} = DC\epsilon, \quad (3)$$

1.2.1. Non-Linear Shear Damage Model

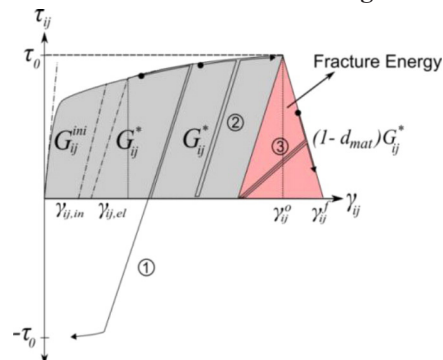


Figure 4. Non-linear shear model.

Considerable plastic shear strain and modulus degradation was observed with several consecutive loading-unloading cycles applied to test specimens. Prior to damage initiation, shear loading and unloading occurs along gradients defined by the initial shear modulus G_{ij}^{ini} and degraded shear modulus G_{ij}^* , shown in Figure 4. According to plastic damage theories, the plastic strain

represents all irreversible deformations including those caused by matrix microcracks. The shear strain γ_{ij} was decomposed into the elastic part $\gamma_{ij,el}$ and the inelastic/plastic part $\gamma_{ij,in}$,

$$\gamma_{ij} = \gamma_{ij,el} + \gamma_{ij,in}, \quad i \neq j = 1, 2, 3 \quad (4)$$

The elastic strain is given by,

$$\gamma_{ij,el} = \tau_{ij} / G_{ij}^* \quad (5)$$

The stress-strain constitutive laws are represented by an exponential function,

$$\tau_{ij}(\gamma_{ij}) = \begin{cases} \tau_{ij}^Y * [\exp(\alpha * \gamma_{ij}) - \exp(\beta * \gamma_{ij})], & \gamma_{ij} \geq 0 \\ \tau_{ij}^Y * [-\exp(-\alpha * \gamma_{ij}) + \exp(-\beta * \gamma_{ij})], & \gamma_{ij} < 0 \end{cases} \quad (6)$$

where τ_{ij}^Y is the initial yield strength, determined by the 0.2% offset strain point.

1.2.2. Damage Initiation

A strain based damage initiation function was used, for simplicity, to model the material response in the longitudinal direction. The failure initiation criterion based on Puck and Schürmann's [14] and Catalanotti et al. [15] was used for predicting matrix damage behaviour. The failure criteria for fibre-dominated and matrix-dominated initiation modes are given as follows,

$$\text{Fibre-dominated} \quad \varepsilon_{11} > 0, F_{11}^T(\varepsilon_{11}) = \left(\frac{\varepsilon_{11}}{\varepsilon_{11}^{OT}} \right)^2 \geq 1 \quad (7)$$

$$\varepsilon_{11} < 0, F_{11}^C(\varepsilon_{11}) = \left(\frac{\varepsilon_{11}}{\varepsilon_{11}^{OC}} \right)^2 \geq 1 \quad (8)$$

$$\text{Matrix-dominated} \quad \sigma_{NN} \leq 0, F(\theta) = \left(\frac{\tau_{LN}}{S_{12} - \mu_{LN} \sigma_{NN}} \right)^2 + \left(\frac{\tau_{NT}}{S_{23} - \mu_{NT} \sigma_{NN}} \right)^2 \quad (9)$$

$$\sigma_{NN} > 0, F(\theta) = \left(\frac{\sigma_{NN}}{S_{23}} \right)^2 + \left(\frac{\tau_{LN}}{S_{12}} \right)^2 + \left(\frac{\tau_{NT}}{S_{23}} \right)^2 + \lambda \left(\frac{\sigma_{NN}}{S_{23}} \right) \left(\frac{\tau_{LN}}{S_{12}} \right)^2 + \kappa \left(\frac{\sigma_{NN}}{S_{23}} \right) \quad (10)$$

Parameters κ and λ are given by $\kappa = S_{23}^2 - (Y^T)^2 / S_{23} Y^T$, $\lambda = 2\mu_{LN} S_{23} / S_{12} - \kappa$, S_{12} and S_{23} are the shear strengths. The transverse friction coefficients, defined in [13], are based on Mohr-Coulomb theory where $\mu_{NT} = -1/\tan(2\theta_f)$, $S_{23} = Y^C/2 \tan(\theta_f)$ and $\mu_{LN} =$

$\mu_{NT} S_{12} / S_{23}$, Y^C is the transverse compressive strength and the initiation strain $\varepsilon_{11}^{OT(C)} = X^{T(C)} / E_{11}$. The fracture plane orientation, θ_f is typically found to be approximately 53° for unidirectional composites under uniaxial transverse compressive loading [13] but may assume any orientation under combined loading.

1.2.3. Damage Evolution

As shown in Figure 5, two parameters are introduced to describe the matrix-dominated damage propagation parameter, d_{mat} , under shear loading: (i) shear damage in the strain hardening part, d_{ij}^I , and (ii) shear damage in the strain softening part, d_{ij}^{II} .

$$d_{mat} = d_{ij}^I + d_{ij}^{II}, \quad (11)$$

$$d_{ij}^I = 1 - G_{ij}^* / G_{ij}^{inI}, \quad (12)$$

$$d_{ij}^{II} = d_{ij}^I + (1 - d_{ij}^I) \frac{\gamma_{ij}^f - \gamma_{ij,in}^o}{\gamma_{ij}^f - \gamma_{ij}^o} \left(\frac{\gamma_{ij} - \gamma_{ij}^o}{\gamma_{ij} - \gamma_{ij,in}^o} \right), \quad (13)$$

where γ_{ij} is the current shear strain, γ_{ij}^f is the final failure shear strain, and $\gamma_{ij,in}^o$ is the inelastic strain at the onset of fracture.

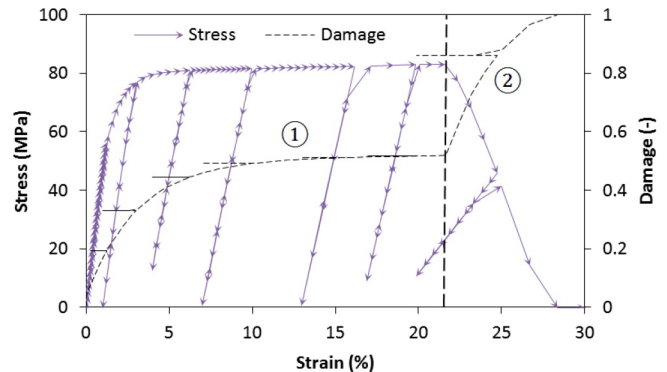


Figure 5. Perfect-plastic stress-strain constitutive law for non-linear shear.

In order to account for irreversibility, the damage variable, as a function of analysis time, t , is defined as

$$\begin{cases} d_{mat}(t + \Delta t) = \max\{0, \min\{1, d_{mat}(t + \Delta t)\}\} \\ d_{mat}(t + \Delta t) \geq d_{ij}(t) \end{cases}, \quad (14)$$

As damage is triggered when the shear strength, τ_{ij}^o , is reached, the response follows a negative tangent stiffness resulting in the softening of the secant shear modulus, with increasing applied strain, to $(1 - d_{mat})G_{ij}^*$, shown by path 3 in Figure 4. Consequently, final failure strain, γ_{ij}^f , is determined by,

$$\gamma_{ij}^f = \frac{2g_{ij}}{\tau_{ij}^o} + \gamma_{ij,in}^o, \quad (15)$$

where $\gamma_{ij,in}^0$ is the plastic strain at the onset of failure. Mesh objectivity was achieved by employing the crack-band model of Bažant and Oh [16], where a characteristic length of the finite element (equivalent to a RVE), $g_{ij} = \Gamma_{ij}/l^*$, l^* and the corresponding fracture toughness Γ_{ij} were used. g_{ij} is the volumetric energy release rate associated with elastic fracture energy (red area in Figure 4). A quadratic interpolation function for the fracture energy in the mix-mode case was described in [6] to account for the multidirectional loading cases. An accurate measure of the characteristic length is the ratio of the elemental volume V and fracture plane area A , $l^* = V/A$, described in [6].

A mesh sensitivity study, on a 2mm×2mm×2mm cube loaded in longitudinal tension, was performed with 1^3 , 2^3 , 3^3 , 4^3 and 5^3 elements respectively. The models were loaded in tension longitudinally until complete failure. The response in Figure 6 confirmed the mesh independence of the proposed model as the force-displacement curves of the different meshes were in good agreement except the point close to complete failure. The deviation near complete failure is caused by the difference between the infinitesimal strain used in the model and the finite deformation formulation used in the FE package. This difference is only significant where there are large strains experienced by small elements nearing complete failure.

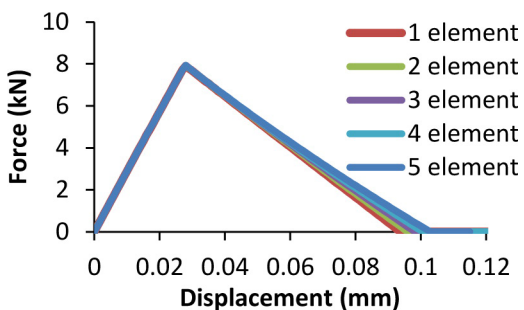


Figure 6. Force-displacement curve for cube model with different mesh densities

1.3. Implementation of Intralaminar Damage Model

The developed intralaminar damage model assesses damage in the two phases of a continuous fibre unidirectional composite ply. Fibre-dominated damage is primarily associated with loading along the fibre direction. The anticipated damage will occur in the form of net fibre breakage in tension and predominantly fibre kink band formation when loaded in compression. Matrix-dominated damage is primarily associated with transverse and shear loading, which leads to plasticity and formation of cracks in the matrix material. The use of a continuum damage mechanics based softening constitutive relationship necessitates the determination of a characteristic length to correctly scale the critical energy release rate.

Element deletion was employed in this model due to the softening nature of the constitutive relationships. An efficient strategy for determining when element deletion is likely to have to be invoked, due to element distortion, is to track the value of the determinant of the Jacobian operator ($\det \mathbf{J}$) [6] and delete the element if this value approaches zero. As $\mathbf{J}$ is not available directly from ABAQUS/

VUMAT, and adding this calculation to the subroutine would incur an additional computational cost, the strategy adopted was to interrogate the determinant of the deformation gradient ($\det \mathbf{F}$) which is available directly from ABAQUS. $\det \mathbf{F}$ yields the ratio of the deformed, V , and undeformed, V_0 , volume of an element,

$$\det \mathbf{F} = \frac{V}{V_0}, \quad (16)$$

and provides a reasonable indication of element distortion. The overall element distortion criterion was subsequently based on both the fibre-dominated longitudinal damage parameter, $d_{11}^{T(C)}$, and limits on $\det \mathbf{F}$ for tracking large changes in element volume,

$$\begin{cases} \text{Delete element if} \\ d_{11}^{T(C)} > 0.99 \\ \det \mathbf{F} < 0.8 \text{ or } \det \mathbf{F} > 1.6 \end{cases} \quad (17)$$

The limits on $\det \mathbf{F}$ are user defined and the quoted values were found to yield reliable computational stability.

2. IMPACT AND CAI STRENGTH OF THERMOSET COMPOSITES

Low-velocity impact damage can drastically reduce the residual strength of a composite structure even when the damage is barely visible. The ability to computationally predict the extent of damage and Compression-After-Impact (CAI) strength of a composite structure can potentially lead to the exploration of a larger design space without incurring significant time and cost penalties.

2.1. Material and Method

This section describes the application of the developed material model to simulate a low-velocity impact event follow by a compression-after -impact test. In order to demonstrate the effectiveness of the material model as a virtual testing tool, its performance in predicting the response of impact and residual strength was evaluated against published experimental data [17]. The successful prediction of the quantitative and qualitative responses for these test cases gives confidence in the utility of the model for its intended purpose.

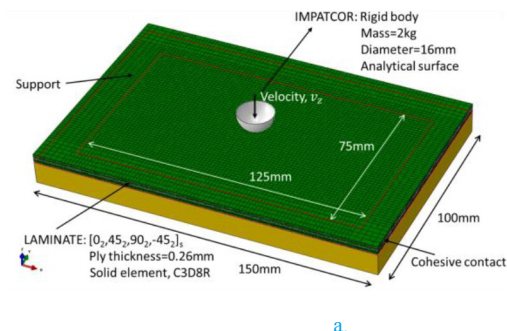


Figure 7. Schematic of (a) Impact and (b) CAI test setups