EXPERIMENTAL AND NUMERICAL ANALYSIS ON DELAMINATION GROWTH IN DAMAGED COMPOSITE MATERIAL

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ABSTRACT

The influence of intralaminar damage on interlaminar damage in composite laminate is studied is this paper. A new fracture mechanics test has been proposed in order to measure the growth of delamination in presence of transverse cracks and local delamination in cross-ply laminates. The higher the transverse cracking rate is, the higher delamination rate experimentally measured is. A FEA "virtual test" campaign of this fracture mechanic test with cohesive zone modelling has been performed in order to simulate the delamination growth. Due to the computational cost, the parameter identification of the cohesive behaviour law for each experimental intralaminar damage state has been performed by a statistical approach thanks to a surrogate model of the FEA results. The evolution of these parameters as a function of the ply damage demonstrates a decrease of the apparent toughness of the interface and underlines the necessity to take into account the influence of transverse cracks and local delamination on the modelling of the delamination growth in composite structure.

1. INTRODUCTION

Composite laminate materials present very interesting alternative solutions to metallic materials due to their better specific properties. Nevertheless these materials develop damage for low applied load. In order to increase the use of composite material in aeronautical structures, it is necessary to improve the knowledge and the modelling of damage mechanisms (obviously the effects of damage on mechanical properties but also their evolution and their influence on the final fracture). The scenario of damage is now well established. Firstly, damages at the microscopic scale like fibre/matrix interface rupture or matrix microcracks appear. Due to the coalescence of these microdamages through the ply thickness, transverse cracks occur. At the transverse crack tip, local delamination could also be initiated due to the stress concentration. The final rupture of the laminate depends on the stacking sequence and could be the consequence of fibre breakage or macro delamination (initiated on local delamination or by free edge effects) [1]. Due to a lack of comprehension of the effect of local delamination, this study proposes a new fracture mechanics test in order to quantify the influence of the ply damage on the macroscopic delamination propagation. In the first part, the experimental methods are presented. In a second part, the numerical model and the virtual campaign are detailed. In the last part, the comparison between numerical and experimental results is discussed.

2. EXPERIMENTS

The fracture characterization of delamination is defined by the toughness of the interface in a fracture mechanics point of view [2]. These material parameters are measured by classical fracture mechanical tests like DCB (double cantilever beam), ENF (end notch flexure) or MMF (mixed mode flexure). Although these tests give accurate results, they can't permit to study the influence of intralaminar damage and more precisely of local delamination on the delamination growth. These tests need to insert film in order to generate starter crack during the manufacturing process that does not allow damaging the composite preliminary by another classical mechanical test (tensile or bending tests).

Therefore, a new experimental procedure has been developed. In a first step, a tensile test is performed on cross ply laminate ($[0_m/90_n]_s$) in order to have a specified damage state in the 90° plies as it has been determined in previous studies [3]. Thanks to a specific experimental device with a digital video microscope, all types of damage in the gauge section of the specimen are monitored under loading at the ply scale and at the fibre/matrix scale. It permits to measure not only the transverse cracking rate but also the length of local delamination present at the tip of the transverse cracks (Figure 1).

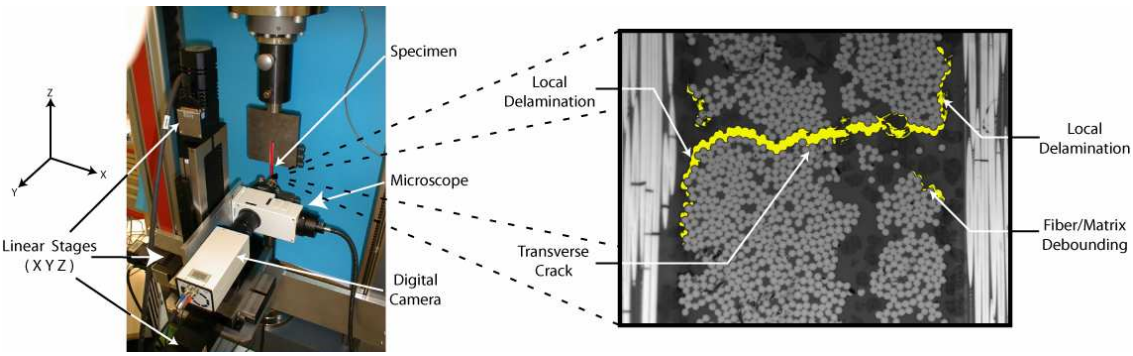

Figure 1: Presentation of the experimental procedure and the type of damage monitored.

In order *(i)* to monitor easily and accurately the damage state and *(ii)* to provide a good compromise between numbers of transverse cracks and length of local delamination, the chosen stacking sequence is a $[0_2/90_2]_s$. All specimens are 240 mm long, 16 mm width, with 100 mm gauge section.

In a second step, a notch (0.5mm) in the 0° plies at the middle of the coupon is performed in order to introduce locally a tensile-bending coupling. In a third and last step, another tensile test is performed on the notched damage specimen in order to firstly create a transverse crack under the notch leading to a delamination and secondly propagate this delamination (Figure 2).

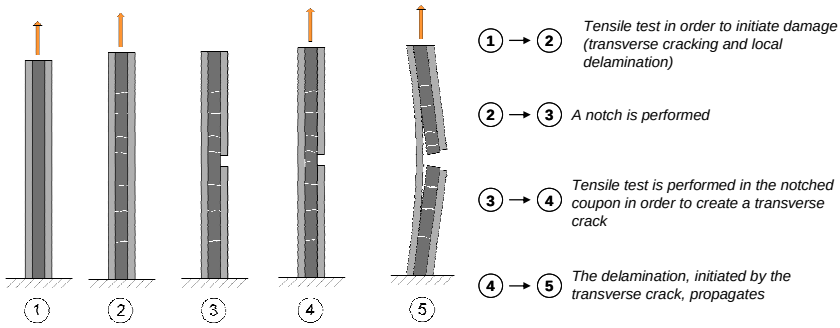

Figure 2: General principle of the proposed experimental procedure.

The experimental device presented previously permits to measure the delamination length as a function of the longitudinal displacement applied to the cross-ply laminate. The evolutions of the delamination length and the average laminate stress as a function of the imposed displacement are plotted on Figure 3 for several damage states. It is important to notice that in Figure 3a the delamination propagates at a constant stress (noted later critical stress level). The higher the transverse cracking rate (ρ) is, the higher delamination rate is as a function of the imposed displacement. However the critical stress level decreases as a function of the transverse cracking. It has been also insured that for the imposed displacement, any other transverse crack appears or local delamination propagates.

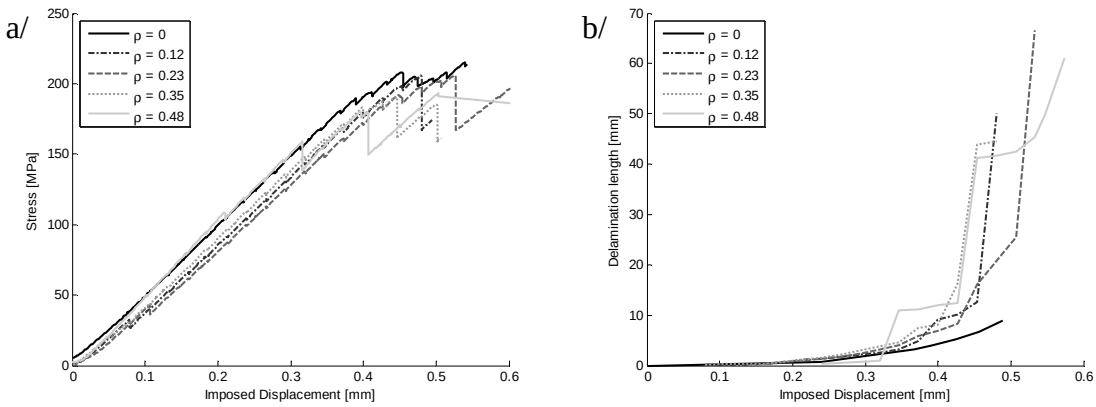

Figure 3: Evolution of the stress and delamination length as a function of the imposed displacement for several damage state.

3. VIRTUAL TEST CAMPAIGN

In order to analyze the experimental results, a virtual test campaign has been performed thanks to finite element computations with the FEA solver ZéBuLoN. Due to the stacking sequence, 2D computations have been performed with small deformation plain-strain and with high displacement hypothesis. A preliminary study has demonstrated that in the experimental precision on the notch manufacturing, its size and its position have no effect on the delamination length computed. Hence, due to the symmetry of the structure and loading conditions, computations have been performed only on one half of the coupon. The finite element mesh and the boundary conditions of the numerical problem are presented on Figure 4. The displacement is imposed all over

the laminate on the rigth side and only the longitudinal displacements of the unnotched 0° plies are fixed on the left side in order to simulate the central transverse cracking and the flexure of the coupon.

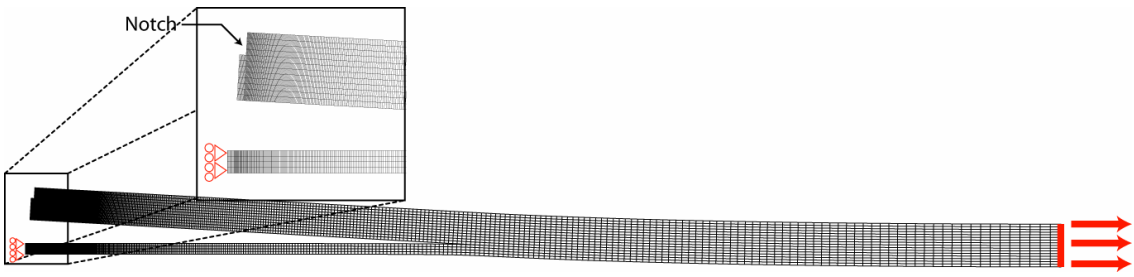

Figure 4: Deformed finite element mesh and boundaries conditions

Each ply is considered as an elastic transversely isotropic material. Thanks to a previous study on the effect of transverse cracking and local delamination on the elastic behaviour of damaged ply [3], the equivalent elastic compliance tensor of the 90° plies $\underline{\underline{\tilde{S}}}$ is determined for each level of the normalized crack density ($\bar{\rho}$) and level of the associated local delamination ratio ($\bar{\mu}$):

$$\underline{\underline{\tilde{S}}} = \underline{\underline{S}}^0 + \bar{\rho}\underline{\underline{H}}(\bar{\rho},\bar{\mu}) \qquad (1)$$

where $\underline{\underline{S}}^0$ is the initial compliance tensor of the ply and the damage effect tensor $\underline{\underline{H}}$ is defined by:

$$\underline{\underline{H}}(\bar{\rho},\bar{\mu}) = \underline{\underline{H}}_1^{\bar{\rho}} + \bar{\rho}\underline{\underline{H}}_2^{\bar{\rho}} + \bar{\mu}\underline{\underline{H}}^{\bar{\mu}} \qquad (2)$$

The identification of $\underline{\underline{H}}_1^{\bar{\rho}}, \underline{\underline{H}}_2^{\bar{\rho}}$ and $\underline{\underline{H}}^{\bar{\mu}}$ is performed by virtual testing [3].

The cohesive behaviour law proposed by Tvergaard [4] has been used to compute the delamination growth in the laminate. In such behaviour law, the relationship between the relative normal and tangential displacement at the interface (u_n, u_t) and their corresponding tractions components (T_n, T_t) depends on a damage variable λ and its generic form is:

$$\begin{cases} T_n = E\dfrac{u_n}{\delta_n}F(\lambda) & \text{if } u_n \geq 0 \\ T_n = KE\dfrac{u_n}{\delta_n} & \text{if } u_n < 0 \end{cases} \quad \text{and } T_t = \alpha E\frac{u_t}{\delta_t}F(\lambda) \qquad (3)$$

Where δ_n and δ_t are the normal and tangential displacements at the complete separation for pure normal and tangential modes. $E, \alpha E$ and KE are the initial stiffness of the interface for tensions, shear and compressive loading. In the Tvergaard model, the damage parameter is defined as the maximum value of the norm of the displacement and the function $F(\lambda)$:

$$F(\lambda) = (1-\lambda)^2 \text{ with } \lambda = \max_{\tau<t} \sqrt{\left\langle \frac{u_n}{\delta_n} \right\rangle^2 + \left(\frac{u_t}{\delta_t} \right)^2} \qquad (4)$$

In Eq. (4), the Mc Cauley bracket $\langle \ \rangle$ indicates that the negative normal displacement has no effect on the damage evolution. In order to avoid "solution jump" problem [5], a dynamic analysis has been performed.

The aim of this virtual test campaign consists in identifying the parameters of the interface behaviour law assuming the behaviour of the ply is well known. The whole identification process is based on an optimization scheme which needs an important number of computation results. Due to finite element model hypotheses (dynamic analysis, small deformation but large displacement, cohesive zone model), computation cost of such identification approach is too high. An alternative choice is in a first step, build a surrogate models thanks to an effective design procedure on the numerical data obtained from finite element simulations and in a second step, perform the identification with this surface response approach [6].

The design procedure has been defined by a low-discrepancy sequences approach [7] on six parameters of the finite element model ($\overline{\rho}, \overline{\mu}, \delta_n, \delta_t, \sigma_{\max}$ and α). These parameters characterise both the damage state in 90° plies ($\overline{\rho}, \overline{\mu}$) and the behaviour of the interface ($\delta_n, \delta_t, \sigma_{\max}$ and α). The range of variation of these parameters is mentioned in Tab. (1).

	$\overline{\rho}$	$\overline{\mu}$	δ_n	δ_t	$\sigma_{\max}$	α
Lower Bound	0	0	5	1.e-2	1.e-2	1
Upper bound	0.6	1	80	5e-2	5.e-2	3

Table 1: Range of the design parameters space

The training phase of the neural network [8] which is the present surrogate model is carried out by an half (50 simulations) of the virtual experimental design computation results. The validating phase of the neural network has been successfully performed by comparison between the other half of computation results (50 other simulations) and the surrogate model responses (cross-validation).

Hence, for a given set of design parameters, this surrogate model is able to simulate the evolution of the delamination length and the applied stress as a function of the imposed displacement.

Thanks to this surrogate model, it is now possible for numerous set of design properties and for a very low computational cost to have a description of the density of the simulated response of the applied stress and delamination length (Figure 5).

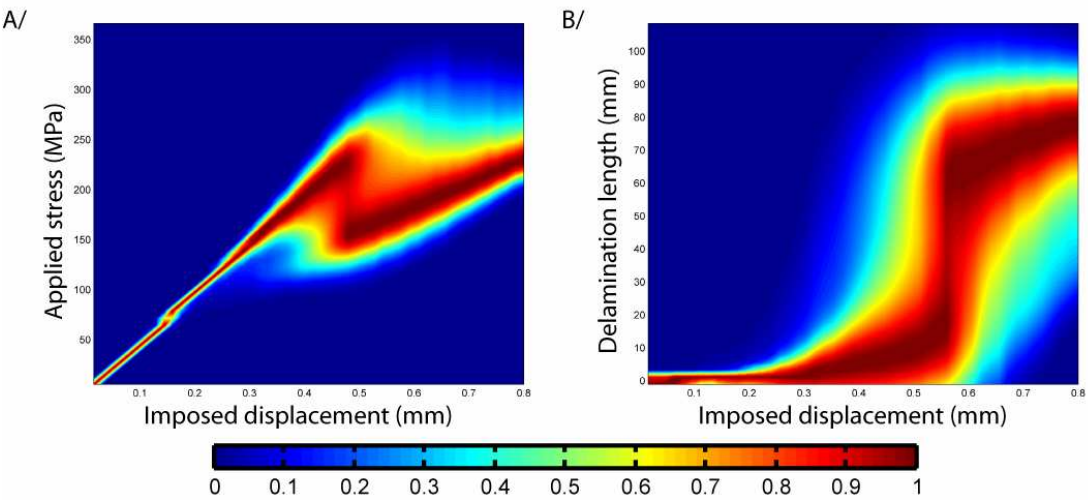

Figure 5: Density of applied stress response (A/) and delamination length evolution (B/) as a function of the imposed displacement for a variation of the cohesive law parameters.

The fact that several set of parameters could give very macroscopic similar response is underlined by Figure 5. This remark implies that during the identification process, the solution determined by a classical optimization scheme is not necessary better than an other set of parameter. Therefore a robust identification procedure has been defined thanks to a statistical approach.

For each experimental 90° plies ply damage state ($\overline{\rho}, \overline{\mu}$), numerous responses have been simulated by the surrogate model by varying interface damage parameters. The quadratic error (r_i) to the experimental curve has been calculated for each simulation. This quadratic error permits to define a weighting (p_i) for each simulation which permit to calculate weighted average ($\overline{x}$) and variance (σ^2):

$$\begin{cases} \overline{x} = \dfrac{\sum_i p_i x_i}{\sum_i p_i} \\ \sigma^2 = \dfrac{\sum_i p_i (x_i - \overline{x})^2}{\sum_i p_i} \end{cases} \text{ with } p_i = 1 - \frac{r_i}{\sum_i r_i} \qquad (5)$$

With this approach, all simulations are used for the identification of the cohesive zone model parameters and their couplings for each damage ply level. The Figure 6 plots the value of the weight as a function of $\sigma_{\max}$ and δ_n for a 90° ply without damage. A relation between these two parameters can be identified. A high δ_n (respectively $\sigma_{\max}$) could counterbalance a low $\sigma_{\max}$ (respectively δ_n). This figure as the Figure 5 underlines the non-unicity of the solution.

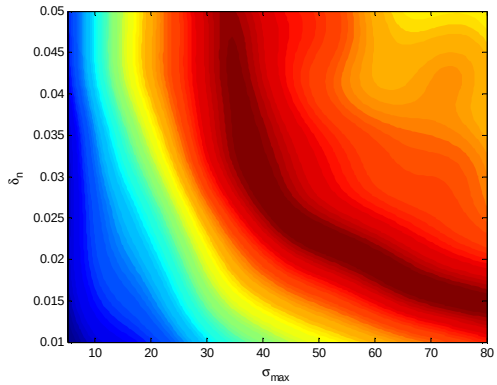

Figure 6: Evolution of the weighting as a function of $\sigma_{\max}$ and δ_n for the coupon without damage in 90° plies

4. DISCUSION

Thanks to Eq. (3), the critical energy release rates necessary to have a complete separation for pure normal (G_{Ic}) and tangential modes (G_{IIc}) are defined by the following relations for the Tvergaard cohesive behaviour law:

$$\begin{cases} G_{Ic} = \int_0^{\delta_n} T_n \, du_n = \dfrac{9}{16} \delta_n \sigma_{\max} \\ G_{IIc} = \int_0^{\delta_t} T_t \, du_t = \dfrac{9}{16} \alpha \delta_t \sigma_{\max} \end{cases} \tag{6}$$

The evolution of the weighting (Eq. 5) as a function of G_{Ic} and G_{IIc} for several experimental damage ply level is presented on Figure 7. The value of G_{IIc} identified by our identification procedure ($\overline{G}_{IIc}$=1580 J/m²) are in good agreement with other experimental results [9]. For the G_{Ic}, the value identified ($\overline{G}_{Ic}$=798 J/m²) is higher than the experimental results. Nevertheless, the toughness measured in this study concerned an 0°/90° interface and not a standard 0°/0° interface. Allix et al. [10] have demonstrated for another composite/epoxy material an increase of the G_{Ic} for $\pm\theta$ interface that could explain our results.

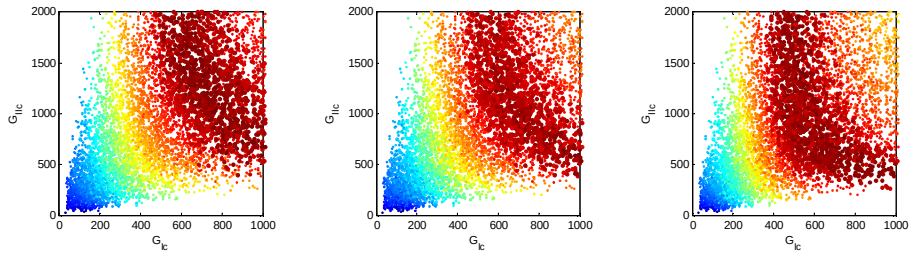

Figure 7: Evolution of the weighting as a function of G_{Ic} and G_{IIc} for several experimental damage ply level (A/ no damage, B/ $\overline{\rho}$=0.23, $\overline{\mu}$=5.e-2, C/ $\overline{\rho}$=0.48, $\overline{\mu}$=10.e-2)

By increasing the damage state, it has been observed a decrease in the best identified critical energy rate (highest weight). Due to the mixed-mode delamination growth in this experimental test, a relation similar to $\sigma_{\max}$ and δ_n is observed between G_{Ic} and G_{IIc}. The decrease of G_{Ic} and G_{IIc} is confirmed by the Figure 8 in which the evolution of the weighted average of the energy release rates as a function of the experimental damage ply state is plotted. When the damage in the 90° plies increases, G_{Ic} and G_{IIc} decrease down to 10%. It is important to notice that the effect of damage on the 90° ply elastic behaviour is taken into account thanks to Eq. (1). Hence, this decrease is only due to the evolution of the cohesive behaviour of the interface.

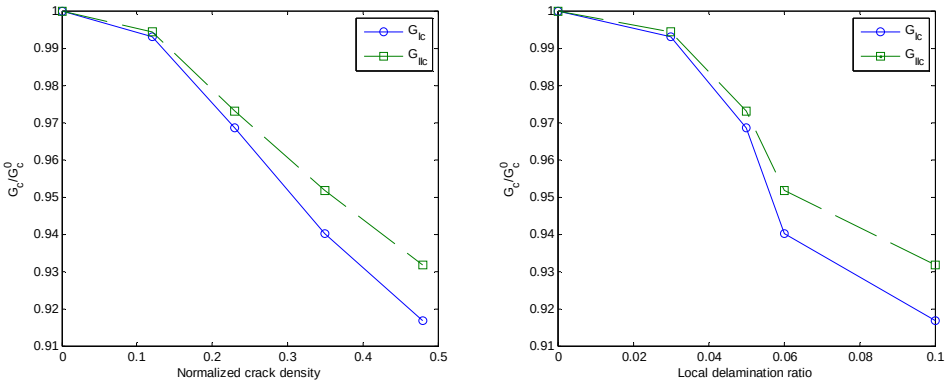

Figure 8: Evolution of the normalized G_{Ic} and G_{IIc} as a function of the normalized crack density ($\overline{\rho}$) and the local delamination ratio ($\overline{\mu}$)

5. CONCLUSION

In order to study the delamination growth of composite laminate in presence of damage in the ply, a specific experimental procedure has been proposed. Unlike classical fracture mechanic tests, this experimental procedure permits to damage in a first step the laminate and then propagates the delamination in presence of local delamination. The experimental campaign demonstrates that the more important the damage ply is, the faster the kinetic of delamination is.

A surrogate model based on finite element simulations of the experimental procedure permits to demonstrate by an inverse identification of interface behaviour law parameters, a decrease of the apparent toughness of the interface. Since the effects of damage (experimentally measured) on the ply behaviour are taken into account by our finite element model, this decrease is only due to the influence of local delamination on the delamination growth. These numerical and experimental results are in agreement with the “virtual testing” approach leads by Ladeveze *et al.* [11].

These results demonstrate the influence of the transverse cracks on the local delamination and could explain the difficulty to simulate the delamination growth in singular zones where other damages are observed (holes, edge of plates, bolted joints problem, impact).

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