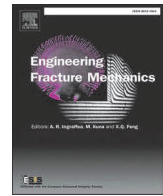




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Analysis and experimental characterization of static and fatigue delamination in L-shaped laminates: Towards slow-growth damage tolerance

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ABSTRACT

The development of slow-growth design approaches for the fatigue and damage tolerance of composite aircraft structures containing pre-existing damage remains a major challenge to this day. Delamination is particularly critical, due to difficulties in detection and the high sensitivity of laminated composites to this damage mode. The aim of this work is to advance the adoption of slow-growth approaches to damage tolerance by investigating propagation regimes in thick, pre-delaminated L-shaped laminates, and to propose numerical approaches to predict damage growth under quasi-static and fatigue loading. A cohesive zone-based finite element fatigue modelling framework is implemented in an explicit finite element code and verified by modelling standard fracture specimens. The method is subsequently applied to simulate the quasi-static and the fatigue response of L-shaped specimens exhibiting both stable and unstable delamination regimes. The evolution of energy release rate with crack growth is analysed with a semi-analytical approach to interpret the observed propagation phenomena. The numerical-experimental correlation suggests that the method presented can elucidate the delamination propagation under cyclic and monotonic loading, in a variety of significantly more complex scenarios than those of characterization coupons.

1. Introduction

Laminated composite structures are particularly vulnerable to out-of-plane loads and low-velocity impacts, which can induce delaminations at loads far below those causing in-plane failure, thus disrupting load transfer between plies and significantly reducing stiffness, compressive strength, and fatigue resistance. Because of the hidden nature of such damage and its direct effects on the structural integrity and inspection requirements of composite aircraft components, delamination is often a central concern during the certification process. The damage tolerance of composite structures and their exceptional sensitivity to impact damage were first mentioned in the FAA AC 20–107B [1]. The Advisory Circular treats the compliance to damage tolerance requirements with specific reference to “Category 2” type damage, defined as the type of damage “that can be reliably detected by scheduled or directed field inspections performed at specified intervals” [1]. Category 2-type damage can reduce structural strength below Ultimate Load (UL), albeit never below Limit Load (LL), and may not be readily visible, but can be detected by scheduled inspections. Examples of such damage

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include visible impact damage (VID), manufacturing mistakes not identified by the end of production, and detectable delaminations. Three approaches are proposed in the Advisory Circular for demonstrating compliance with damage tolerance specifications: no-growth, arrested-growth and slow-growth. According to the no-growth philosophy, damage may not propagate under service loading for the duration of an inspection interval, at which point it is to be detected, repaired, and UL restored. As opposed to no-growth, slow-growth structures do allow for certain types of Category 2-type damage to propagate if the growth rate can be shown to be slow, stable, and predictable.

The no-growth approach is the most conservative of the three, as structural design is conducted in such a way that stresses are below fatigue propagation thresholds in the presence of Category 2 damage, thus ensuring no growth will occur under load cycling (at least within the given inspection interval). Such a restrictive level of conservatism comes with a significant weight penalty, as structures must be oversized until stresses are below propagation thresholds at all points. Despite this disadvantage compared to slow-growth, the no-growth concept has remained the standard for the substantiation of damage tolerance of composite aeronautical structures. Stresses above the infinite-life threshold would automatically mean the growth of existing damage under service loading, which is allowed only in slow-growth designs. In no case is any damage allowed to propagate so much as to lower residual strength below LL.

Therefore, for the design of slow-growth composite structures, designers must have at their disposal analysis tools – e.g., Finite Element (FE) models – for the prediction of the residual static strength in the presence of pre-existing damage, and the damage growth rate. With the aid of such tools, critical damage sizes and inspection intervals could be accurately determined, and inspection programs generated.

Numerical methods for static failure prediction of delamination, especially those based on the Cohesive Zone Modelling (CZM) of interface, are now mature and well established [2–6], but the reliable prediction of fatigue-driven growth rates still requires significant development. Therefore, the core of this work focuses on evaluating a fatigue-growth modelling approach for predicting delamination under realistic mixed-mode loading. Providing reliable predictive models would remove a roadblock to the slow-growth substantiation of damage tolerance and unlock the associated structural weight savings. Other challenges to the implementation of slow-growth design remain to be addressed beyond the modelling aspect, including the inspection method [7]. However, the focus of the present work consists of the modelling aspect of the approach.

Previous work on fatigue damage prediction based on FE analyses (FEA) is abundant in the literature [8–13], but the proposed approaches have often been cumbersome in their formulation and calibration. In particular, relying on Paris crack growth rate data as input is expensive and requires long testing campaigns for each new material. Alternatively, the low-input CF20 and CF22 cohesive fatigue models have been shown to possess promising capabilities [14–16]. When used in combination with the Simplified Cyclic Loading (SCL) technique, the CF20/CF22 models can simulate the fatigue damage accumulation with an analysis in which the load (or displacement) is kept constant and the effect of load cycling is accounted for at the constitutive level of the model.

These cohesive models account for the effects of stress ratio, R , and mode mixity by prescribing the evolution of fatigue damage through an empirical function. The models rely on the intrinsic relationship between the fatigue strength to life (S-N) curve and the rates of propagation described by the Paris law of the interface [16]. This FE fatigue simulation framework showed promising results on Double Cantilever Beam (DCB), End-Notched Flexure (ENF) and Mixed-Mode Bending (MMB) specimens, as well as on a skin-doubler specimen, open-hole and multi-directional laminates in tension [15–21].

The goal of the present work is to assess the capabilities of the modelling framework by simulating the quasi-static and fatigue damage progression of a bent (L-shaped) thick laminate containing different sizes of pre-existing delaminations. Curved laminated composites are especially susceptible to this failure mode, as their curvature induces through-the-thickness stress gradients that significantly amplify the transverse tensile stresses at existing interlaminar defects [22–24]. Specimens were tested in traction, which induces interlaminar tension stresses at the edges of the pre-delaminated interface. Although fracture is dominated by mode I, the test setup puts a shear load on the specimen that induces a mode II contribution. Therefore, the stress state at the crack tip is in mixed-

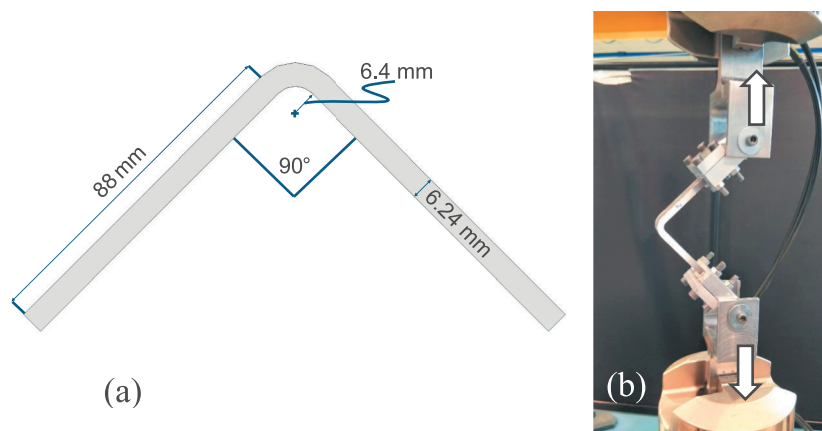


Fig. 1. (a) Nominal specimen dimensions (b), test set-up.

mode.

The following Section describes the static and fatigue tests conducted on pre-delaminated L-shaped specimens. The specimens are tested with two different pre-existing defect sizes: 2 mm (a short defect) and 8 mm (a longer defect). The defect consists of an artificial delamination at a critical interface, created by embedding a Teflon insert during the lamination process. Section 3 presents a semi-analytical characterization of the stability characteristics of crack propagation in these specimens, which is a function of the length of the initial delamination. Section 4 discusses the static and fatigue damage and failure modelling of the tests in Section 2, as well as the characterization of the fatigue model parameters. The main findings and concluding remarks are presented in Section 5.

2. Tests on L-shaped laminates

The experimental testing effort conducted herein consists of quasi-static and fatigue tests of L-shaped laminates with pre-implanted delaminations. The configuration of the test is shown in Fig. 1. The specimens are 20 mm wide, and the load is applied at the end of the specimen arms by gripping the last 30 mm of their length. The testing fixture comprises several interacting components, making the system susceptible to minor initial misalignments. While every precaution is taken to ensure precise specimen alignment, residual settling of the load train can still induce slight non-linearities during the initial loading phase. The dimensions of the specimens, shown in Fig. 1-(a), are consistent with those specified for the ASTM D6415 standard interlaminar tensile strength (ILTS) test [25]. Static tests are conducted in displacement control at a loading rate of 1.50 mm/min. Fatigue tests are conducted in displacement control with a displacement ratio $R = 0.1$ and a frequency of 5 Hz. Maximum and minimum forces and the cycle count are recorded throughout the test.

All specimens are manufactured with AS4/8552 unidirectional (UD) prepreg, with a $[0]_{48}$ layup using the manufacturer-recommended cure cycle. The nominal cured ply thickness is 0.13 mm [26], resulting in a total nominal laminate thickness of 6.24 mm. The elastic and interlaminar properties of the material are reported in Table 1, where G_{Ic} is the mode I critical energy release rate (ERR), and σ_{c0} is the interlaminar tensile strength, and analogously G_{IIc} and τ_{c0} for mode II.

The artificial defect is introduced during the lamination process with a Teflon strip placed at the center of the curved region, as shown in Fig. 2-(a), at one-third through the thickness of the laminate from the interior radius (between the 16th and 17th plies, where interlaminar tensile stresses peak [28]), and spanning its entire width. As already mentioned, specimens having initial delaminations in two sizes are tested: one “short” size, measuring 2 mm, and one “long” size, measuring 8 mm. By size is meant the arc length of the Teflon insert. All tests are conducted on an MTS 858 Mini Bionix II tensile loading machine, equipped with a 15 kN load cell.

2.1. Quasi-Static tests

Static tests are conducted to evaluate how damage initiates and evolves in this type of coupon. The load–displacement curves for two specimens containing the 2-mm initial delamination are shown in Fig. 3. As can be seen from the plot in Fig. 3, the initial linear-elastic phase is followed by a sudden load drop. A comparison between Fig. 3-(a) and Fig. 3-(b), (figures taken before and after the initial propagation event) indicates that the observed load drop corresponds to an unstable delamination growth from the initial delamination, across the curved region of the specimen. Further load increments result in an extension of the crack into the specimen arms, as can be observed in Fig. 3-(c), which induces a reduction in stiffness with further increments in displacement, as can be seen in the force–displacement curves. A degree of variability is observed in the initial failure loads of the two tested specimens; although the sample is too small for a statistical characterization, the observed difference is consistent with the scatter commonly reported for interlaminar failure in curved laminates [29,30]. This scatter can be attributed to a combination of factors, among which are the inherent material variability of strength and toughness properties characteristic of composite interface, manufacturing tolerances (such as a slight misalignment of the Teflon insert during layup and curing), as well as experimental factors, including minor misalignments within the load train assembly. At this point it should be noted that the number of specimens tested at each condition is limited; indeed, the campaign is intended to identify the characteristic propagation regimes and their governing mechanisms, rather than to provide a statistical characterization of their variability. Regarding the nomenclature used to identify each specimen, the first letter stands for the loading type under which it is tested (“F” for fatigue and “S” for static loading), followed by the size, a_0 , of the pre-inserted delamination (“2” for $a_0 = 2$ mm and “8” for $a_0 = 8$ mm). The last two numbers indicate the production batch and the specimen number within it, respectively.

The load–displacement curves of the two specimens with an 8-mm initial delamination are presented in Fig. 4, alongside the corresponding damage progression during the quasi-static tests. Upon closer inspection of the initial response, although it appears to be nonlinear, the nonlinearity is primarily isolated to specimen S.8–1-2. The initial stiffening observed in S.8–1-2 is, as described in the previous section, an artifact of the complex testing fixture—which comprises the grip, hinge, and load fork. Minor misalignments during specimen installation can introduce this apparent nonlinearity as the system components align and settle under the applied load. The first propagation is characterized by a much less significant load drop than that observed in the tests with a shorter initial

Table 1
AS4/8552 elastic and interlaminar properties [26] and [27].

E_{11} [MPa]	$E_{22} = E_{33}$ [MPa]	$G_{12} = G_{13}$ [MPa]	G_{23} [MPa]	$\nu_{12} = \nu_{13}$ [-]	ν_{23} [-]	G_{Ic} [kJ/m ²]	σ_{c0} [MPa]	G_{IIc} [kJ/m ²]	τ_{c0} [MPa]	η_{BK} [-]
127,277	9239	4826	3553	0.302	0.3	0.26	105	0.87	128	1.5



Fig. 2. Focus on the defect: (a) Teflon placement during lamination; (b) zoom on the location.

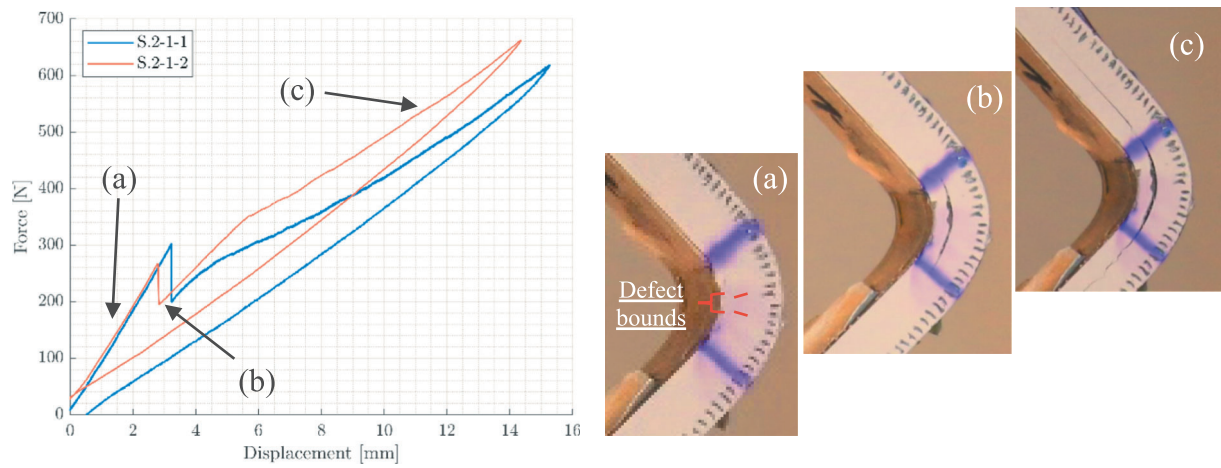


Fig. 3. Static tests on L-shaped specimens with a 2-mm initial delamination.

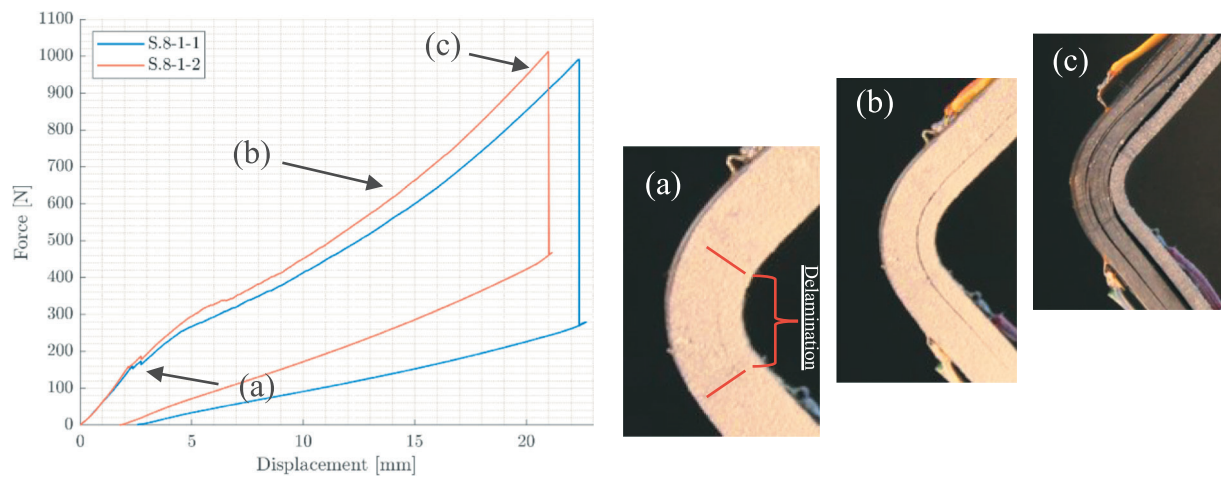


Fig. 4. Static tests on L-shaped specimens with an 8-mm defect size.

delamination, as can be seen in Fig. 4-(a). Upon further loading, the delamination grows into the arms, as is shown in Fig. 4-(b). This continuous growth is reflected by a gradual drop in tangent stiffness, consistent with the behaviour observed in the 2-mm specimens. Both long-defect-size specimens are protracted until a second failure, a violent event accompanied by multiple delaminations in the

upper branch of the specimen at an interface above the pre-delaminated interface, as shown in Fig. 4-(c). This sudden propagation results in the large load drop at the peak-displacement.

In summary, the quasi-static tests highlight distinct failure behaviours depending on the initial defect size. The 2-mm specimens exhibit a linear-elastic initial response followed by sudden, unstable delamination growth. Conversely, the 8-mm specimens undergo a less severe initial load drop. Having established these baseline static responses, the following section presents the experimental results for the fatigue tests conducted on specimens with both sizes of defect, 2 mm and 8 mm.

2.2. Fatigue tests on specimens with shorter initial delamination

Fatigue tests on the specimens with a 2-mm initial delamination are performed on five specimens using displacement control. The initial test severities are determined from the results of the static tests presented in Fig. 3, i.e., as a function of the force at the onset of unstable delamination propagation. The energy release rate, G , can be calculated with the Irwin-Kies equation (Eq. (1)):

$$G = \frac{F^2}{2B} \frac{dC}{da} \quad (1)$$

where F is the applied load, C the specimen compliance in the elastic range, B the specimen width, and a the crack length. Assuming an initial linear elastic response, fatigue loading levels are selected to correspond to a target load severity (defined in terms of initial strain energy release rate), scaled from the critical propagation value F_{crit} under static loading. As a result, the square of the ratio between the applied and critical loads can be used to evaluate the initial test severity in terms of energy release rate ratio, using the relation in Eq. (2):

$$\frac{G_{max}}{G_{crit}} = \text{Severity} = \frac{F_{max}^2}{F_{crit}^2} \quad (2)$$

where F_{max} is the maximum cyclic applied load, and F_{crit} is the force at which propagation from the initial defect occurs. Equation (2) holds since the applied loads in fatigue testing are kept within the linear elastic loading region evaluated in the static tests. Table 2 summarizes the nominal load levels selected for the fatigue tests on the specimens with shorter initial delaminations, and the corresponding maximum displacements and severities. All tests are conducted with a displacement ratio R of 0.1. Two specimens are tested at relatively high severity levels (0.90 and 0.75), and three specimens are tested at lower severity levels (0.62, 0.57 and 0.55). The force levels are adjusted until reaching the desired severity level on each specimen. The severity levels were selected sequentially during the campaign: starting from a high severity of 0.90, the level was progressively reduced from test to test, with the aim of locating the range in which the response transitions from an almost immediate onset of rapid propagation to an extended nucleation phase. Because the tests are conducted under displacement control, the severity is not constant during a test: as the delamination extends and the compliance increases, the maximum force decreases (and with it the available energy release rate). All severity values quoted in this work are therefore intended as initial severities, evaluated for the initial delamination length at the beginning of cycling.

The two specimens that are tested at the highest severities (F.2-1-4 and F.2-1-5 at 0.90 and 0.75, respectively) undergo a rapid delamination propagation within a small number of cycles (41 and 22 load cycles, respectively). The drop of the force shown in Fig. 5-(a) is associated with the rapid initial crack growth from the initial defect length along the curved region. After this event, crack growth rates undergo a significant reduction, and the load settles at a nearly constant maximum load of 189 N and 162 N, for specimens F.2-1-4 and F.2-1-5, respectively. This load drop unfolds within just three load cycles for both specimens, as resolved from the cycle-by-cycle data recorded by the testing machine. After the end of this rapid propagation, subsequent propagation proceeds at a much lower rate.

Specimen F.2-2-3, loaded with a severity of 0.62, is plotted and exhibits a similarly rapid initial crack propagation. Rapid delamination propagation occurs after just 95 load cycles, with a force drop from 164 N to 141 N, unfolding within six load cycles.

The two specimens tested at the lowest severities (in the range from 0.55 to 0.57) exhibit different responses. In the test of specimen F.2-2-4 (0.57 severity), the phase of rapid propagation occurs after 204,000 cycles, and the load drop takes approximately 80 cycles to unfold. After this event, crack propagation enters a smoother, slower-growth regime. The F.2-2-1 test (severity 0.55) exhibits the onset of rapid force reduction at approximately 6000 load cycles. The load drops to about 139 N within 40 cycles, and then transitions to a phase where propagation proceeds at a slower rate.

All specimens exhibit a phase of rapid crack propagation. While a large scatter is observed for tests conducted at nominal severities around 0.60, this high variability is inherent to this specimen geometry and experimental setup, and is consistent with fatigue data reported in the literature for similar configurations [29,30]. Moreover, for test severities of 0.62 and above, rapid propagation is triggered within a few tens of cycles, while for lower severities it occurs after several thousand cycles.

The onset of rapid propagation during fatigue testing is unsurprising considering the quasi-static response of the specimens with a

Table 2

Specimens tested under cyclic loading, the initial test severity and the test maximum displacement ($R = 0.1$, with a 2-mm initial delamination).

Specimen ID	OF.2-1-4	OF.2-1-5	OF.2-2-3	OF.2-2-4	OF.2-2-1
δ_{max}	2.688 mm	2.272 mm	1.870 mm	1.700 mm	1.670 mm
G -severity	0.90	0.75	0.62	0.57	0.55
F_{max}	269 N	222 N	165 N	160 N	159 N

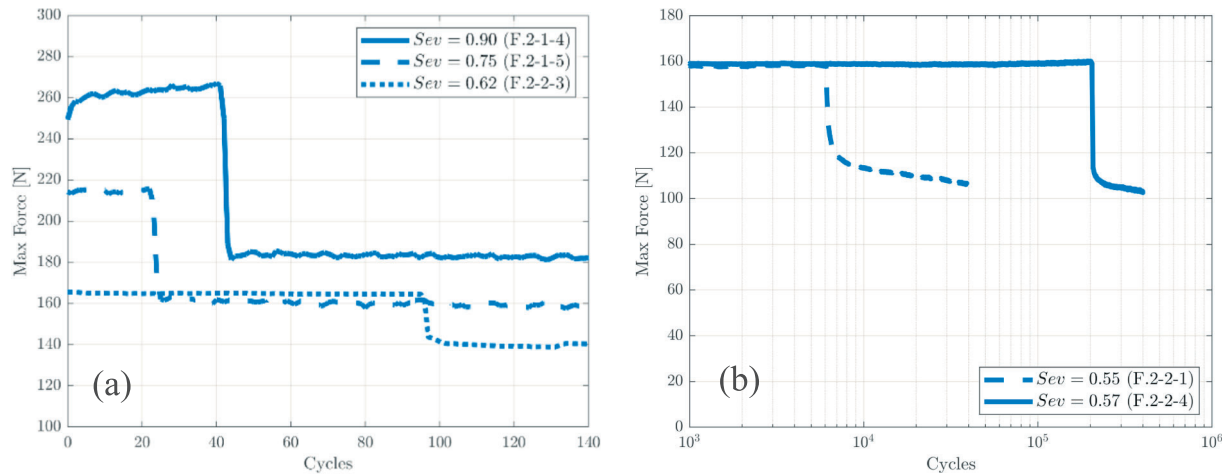


Fig. 5. Max force vs. cycles evolution during L-shaped fatigue tests with a 2 mm initial delamination: (a) tests with a severity greater than 0.60, (b) tests with a severity lower than 0.60.

2-mm initial delamination. In the quasi-static tests, these specimens exhibit an initial unstable propagation from the artificial defect, followed by a transition to stable crack growth. This behaviour reflects a marked reduction in the ERR available for further propagation after this initial event. A similar trend is observed under fatigue loading, where two distinct propagation regimes can be identified. In the first regime, damage accumulates at the pre-existing delamination tip with negligible crack growth until a critical number of cycles is reached. This threshold triggers a rapid delamination propagation across the curved region, accompanied by a pronounced load drop. Unlike the quasi-static case, this load drop is not instantaneous but develops over several load cycles, with an increasing number of load cycles required to trigger propagation as the load severity decreases. Following this unstable phase, crack propagation enters a second regime, characterized by a slower and more gradual crack growth as the delamination continues to extend.

2.3. Fatigue tests on specimens with longer initial delamination

Two specimens are tested with an 8-mm initial delamination at a severity of 0.55. Table 3 reports the corresponding nominal load levels, maximum displacements, and G -severities. The corresponding evolution of the maximum force as a function of cycles is shown in Fig. 6. The two tests show good agreement with one another, with the maximum force differing by less than 2 % throughout the test (105 N and 106.5 N at the beginning; 92.9 N and 94.4 N at 150,000 cycles).

In contrast to the 2-mm specimens, where rapid crack growth is triggered after a finite number of cycles, the 8-mm specimens exhibit a much more gradual response. The delamination propagates slowly and stably from the edge of the Teflon insert, with the applied force decreasing progressively over several thousand cycles until reaching a plateau. No distinct, rapid force drop is observed; instead, the evolution of the force–cycle curve shows a controlled crack propagation, reflective of a more stable degradation process. This response suggests a stabilizing effect associated with the larger initial defect.

A deeper understanding of these propagation phenomena can be obtained by analysing the variation of the energy release rate along the path of the crack, as discussed in the next section. Such a procedure involves an application of the Irwin-Kies equation, which links the compliance of the specimens to the ERR evolution, and, consequently, to crack propagation.

3. Numerical compliance and energy release rate evolution with crack length

A semi-analytical procedure based on the Irwin-Kies equation (Eq. (1)) is performed to evaluate the ERR and the stability of crack propagation for different initial delamination lengths. The equation links the applied load F , the structural compliance C to the ERR (G), i.e., the energy available for crack growth. A FEA is conducted using Abaqus/Standard [31] to determine the compliance of the L-shaped specimen for different crack lengths.

The model consists of a 2-mm-wide strip of the specimen, given uniform geometry and loading across the specimen's width, meshed

Table 3

Specimens tested under fatigue loading, the initial test severity and the test maximum displacement ($R = 0.1$, 8-mm defect).

Specimen ID	0F.8-1-4	0F.8-1-5
$\delta_{\max}$	1.448 mm	1.547 mm
G -severity	0.55	0.55
$F_{\max}$	105 N	106.5 N

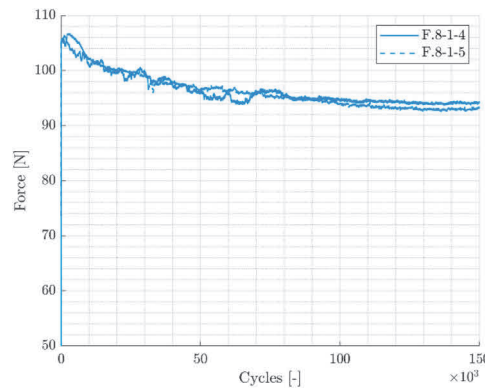


Fig. 6. Max force vs. cycles evolution during L-shaped fatigue tests with an 8-mm defect.

with 12 layers of SC8R laminated continuum shell elements through the thickness, with each layer representing 4 plies (Fig. 7). A layer of zero-thickness COH3D8 cohesive elements containing an initial crack at the centre of the curved region is inserted between the 16th and 17th ply (i.e., between the 4th and 5th layer of continuum shell model). A total of 8 elements is used across the width. The seeding of the arms is biased, which makes the mesh coarser at the extremities of the arms and finer towards the curved region. Elements in the curved region are approximately 0.1 mm long. The material properties used are reported in Table 1. The loading condition is simulated by applying opening displacements to two reference nodes, each coupled to the last 30 mm of each leg through pinned rigid body interactions. The reference nodes are positioned at the hinge locations, and free rotations are allowed around the grip hinge axis, which replicate experimental boundary conditions. Preliminary analyses explicitly modelling the specimen metal grips were conducted to verify this approach; as no significant differences in the model's response were observed, the computationally efficient rigid-body coupling was adopted. A displacement of 2 mm is applied at the hinge locations. An implicit static analysis procedure is used, allowing for a geometrically non-linear response of the model, if present.

The model is used to compute the specimen compliances for different delamination lengths. Cohesive damage is inhibited to ensure that the calculated compliance reflects only geometric effects without the effect of damage propagation. Multiple versions of the same model are generated by removing cohesive elements symmetrically from each tip of the initial delamination in increments of 0.2 mm. The range of analyses span crack lengths from 0 mm to 13.1 mm (arc length of curved region at delamination interface). The results are reported in Fig. 8-(a). The corresponding variation of the ERR, evaluated through Eq. (1), for an applied displacement of 2 mm, is shown in Fig. 8-(b). To validate the use of the Irwin-Kies approach, which presupposes a linear-elastic response to be valid, a parallel evaluation of the ERR was conducted using the Virtual Crack Closure Technique (VCCT). The G vs. a_0 curve extracted via VCCT perfectly overlapped with the semi-analytical Irwin-Kies curve, confirming that the structural response remains linear, and justifying the use of the Irwin-Kies equation.

Assuming a constant crack resistance, G_c , the sign of the derivative of the ERR curve, dG/da , determines the crack propagation regime: unstable if $dG/da > 0$ and stable if $dG/da < 0$. This assumption of a flat R-curve is justified by the fatigue propagation data presented later in Fig. 11-(a). Indeed, the two $R = 0.1$ tests start at different severities but align on the same Paris curve in the linear region, confirming the absence of a significant R-curve effect due to fibre bridging. According to Fig. 8-(b), there is a transition between the unstable and stable regimes at a delamination length.

$a = 6.3$ mm (dashed vertical line in Fig. 8-(b)). Although the curve in Fig. 8-(b) is obtained for a specific opening level, the crack length corresponding to the “stability transition” remains identical for any arbitrary opening displacement. The results of the present analysis are consistent with those of previous studies in the literature [32,33], where a similar approach was used to analyse the stability of delamination propagation in L-shaped laminated specimens [24,25], albeit in slightly different test configurations. It is important to emphasize that Fig. 8-(b) plots the applied crack driving force (G) for a constant opening displacement, rather than the material's fracture resistance (R-curve). Because the absolute values of G in this plot are tied to a static 2-mm opening displacement rather than the specific displacement required to initiate the crack at each length a , the curve is not intended to show where $G \geq G_c$. Instead, its primary purpose is to show the sign of the derivative, dG/da , and assess propagation stability.

The semi-analytical procedure described above allows the interpretation of the experimental results. For the specimens with an

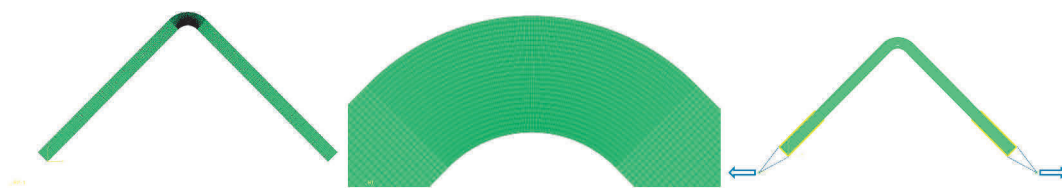


Fig. 7. FE model of the angular specimen.

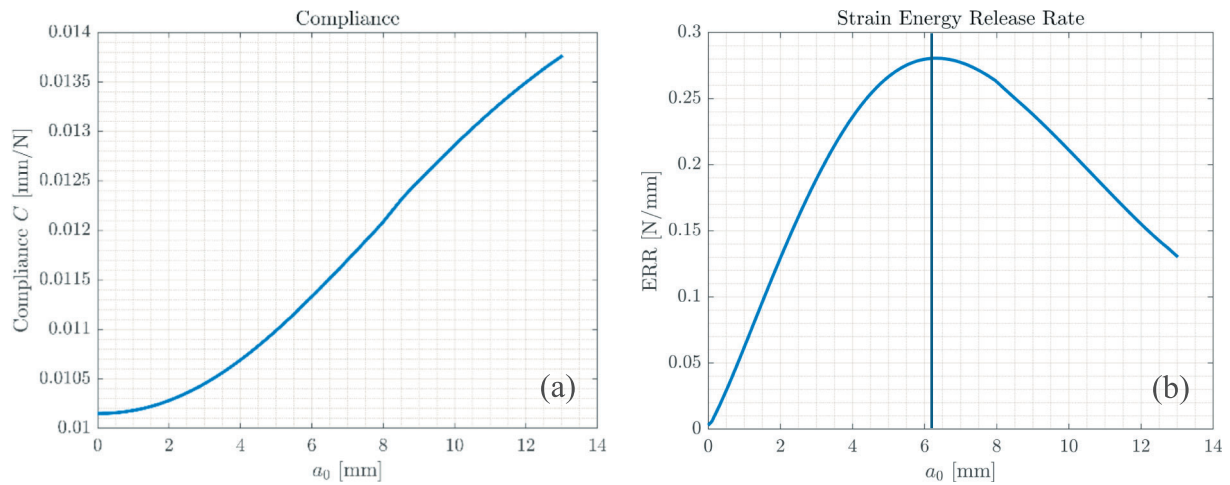


Fig. 8. Numerical compliance (a) and energy release rate (b) of the L-shaped specimen as a function of crack length.

initial delamination length of $a_0 = 2$ mm the crack length at the end of the rapid propagation phase is evaluated using the compliance–to–crack-length relation reported in Fig. 8-(a). For the three tests conducted at the lower severities (0.62 and below), the estimated crack lengths at the arrest of rapid propagation are 7.2 mm for specimen F.2–2-3, 6.0 mm for F.2–2-1, and 7.1 mm for F.2–2-4. These values correlate well with the predicted unstable-to-stable transition crack-length of 6.3 mm corresponding to the peak ERR in Fig. 8-(b). This indicates that the phase of rapid crack growth terminates once the crack enters the region of stable propagation, i.e., after 6.3 mm.

A different behaviour is observed for the two specimens tested at the highest severity levels. For specimen F.2–1-5 (initial severity 0.75), the estimated crack length after the rapid propagation phase is 12.1 mm, a value that is close to the end of the curved region. For specimen F.2–1-4 (tested at an initial severity 0.90), the estimated crack length exceeds the end of the curved region, indicating that the delamination propagates well into the specimen arms. This is the specimen tested at the highest initial severity of the short-defect group, so this response can be attributed to the larger amount of elastic energy stored in the specimen prior to the onset of crack growth, which promotes propagation well beyond the stability transition point. Indeed, for a given crack length, the ERR scales with the square of the applied displacement: at higher severities the entire G–a curve of Fig. 8-(b) is scaled upward, so that a longer crack advance beyond the stability transition is required before the available ERR falls below the level needed to sustain propagation, and the crack arrests at a correspondingly greater length.

As can be seen in Fig. 8-(b), for crack lengths greater than the transition value of 6.3 mm the ERR decreases with crack advancement; the specimens with an initial delamination length of $a_0 = 8$ mm therefore start directly in the stable regime of crack propagation. In this regime, the ERR decreases as the crack length increases, making crack-growth rates decrease as loading progresses, as is normally observed in a displacement-controlled test (at the exception of, for example, displacement-controlled fatigue ENF tests). This prediction is consistent with the slow and steady reduction in force observed during fatigue testing on the long defect-size specimens shown in Fig. 6.

The following section describes the FEA of crack propagation in these L-shaped laminates under quasi-static and cyclic loading. These analyses are particularly challenging due to gradients in ERR discussed in the present section, and the presence of unstable propagation regimes in the quasi-static tests.

4. Analysis of delamination in L-shaped laminates

4.1. Analysis of quasi-static failure

The quasi-static test simulations of the bent laminates are performed using the Simulia Abaqus/Explicit non-linear explicit finite element software with a user-written VUMAT cohesive material model. The same model previously discussed for the compliance calculation is used for the analysis of propagation, with the following differences. First, explicit analysis is used for propagation because it can capture unstable propagation phenomena and transitions to stable regimes without the convergence difficulties that are common with implicit codes [34,35]. The displacement is applied through a smooth amplitude curve to limit the dynamic load oscillations and ensure quasi-static conditions throughout the analysis. A verification is conducted by comparing the reaction forces at the two reference points to ensure they are identical. Another difference between the compliance model and the present propagation model is that the specimens with an 8-mm initial delamination include cohesive interfaces between all layers of elements. These additional cohesive interfaces are needed to capture the multiple delaminations that are observed at the end of the experiments (Fig. 4). The specimens with $a_0 = 2$ mm are not opened until the occurrence of multiple delaminations, so the model retains a single layer of cohesive elements positioned between the 16th and 17th interfaces, where the initial defect is located. Finally, damage in all

cohesive elements, which is disabled in the compliance model, is enabled in the present analysis.

The cohesive zone model (CZM) in [36] is implemented in a VUMAT subroutine for use with COH3D8/COH2D4 cohesive elements in Abaqus/Explicit. Although a similar CZM is available in Abaqus/Explicit, a user-written model is developed for compatibility with the fatigue damage analyses of the following section. The cohesive model has an initial linear-elastic response with a penalty stiffness K_p [31,36]. Damage initiates at the cohesive strength σ_0 and then gradually softens until a displacement δ_f , where the work spent in the damage process equals the prescribed interface toughness. The toughness and cohesive strength values used in the analyses are reported in Table 1 [27]. A quadratic strength criterion for damage initiation, together with the Benzeggagh-Kenane model for damage progression, are selected for the modelling of mixed-mode propagation. The penalty stiffness is set at $1.25E + 05 \text{ N/mm}^3$, following the guidelines proposed in [37].

The predicted and experimental force–displacement responses of the L-shaped specimens with a 2-mm initial delamination are reported in Fig. 9. The model accurately captures the linear-elastic stiffness of the specimen during initial loading and provides a good, albeit slightly under-predictive, estimate of the load at the onset of unstable delamination propagation, which is under-predicted by 12 % on average (251 N in the analysis, against 267 N and 303 N in the tests). The sudden load drop that results from the unstable delamination is also captured by the simulation, as well as the subsequent stable propagation of the crack along the arms of the specimens.

The specimens with the longer initial delamination ($a_0 = 8 \text{ mm}$) are also simulated, this time by modelling the interfaces between every ply with a layer of cohesive elements, so as to be able to capture the second and final failure of the specimen. A comparison between the simulated and experimental results is presented in Fig. 10. The model slightly overestimates the initial stiffness of the specimen, yielding an overprediction of 10 % with respect to the experimental average. However, failure initiation is captured quite well, both in load and in displacement, as well as the response during all subsequent loading, with a final failure load that is overestimated by approximately 25 % with respect to the experimental average. This agreement of the static force–displacement response, including stiffness, first failure load, load drop, and stable crack propagation, is deemed satisfactory and provides sufficient confidence in the capabilities of the modelling approach to reliably predict the quasi-static loads required to propagate initial delaminations in stable and unstable propagation regimes. The model predictions remain smooth and close to the experimental values, even when the crack travels out of the curved region and into the specimen arms, despite the transition in mesh size (Fig. 7).

4.2. CF20 cohesive fatigue model

While the analyses of the previous section verify the cohesive modelling framework under monotonic loading, the simulation of the fatigue tests requires the constitutive treatment of cyclic damage accumulation, which is introduced in this Section.

The accurate simulation of fatigue-driven delamination requires modelling approaches that can capture damage accumulation over a large number of load cycles while remaining computationally efficient. Various numerical strategies have been proposed in the literature to meet this challenge, differing mainly in how they represent the cyclic loading process. Examples of cycle-by-cycle FE fatigue simulations exist in the literature, but these models are only practical for Low and Ultra-Low Cycle Fatigue (LCF, ULCF) situations, which involve very few cycles with large plastic strains, such as seismic loading of steel structures [38]. For High Cycle Fatigue (HCF) applications involving millions of cycles, simulating every load cycle is computationally prohibitive. Therefore, simplified strategies are necessary. Among the available envelope methods for fatigue damage simulation through FE analyses [39], the Simplified Cyclic Loading (SCL) stands out for its computational efficiency and is the approach adopted here.

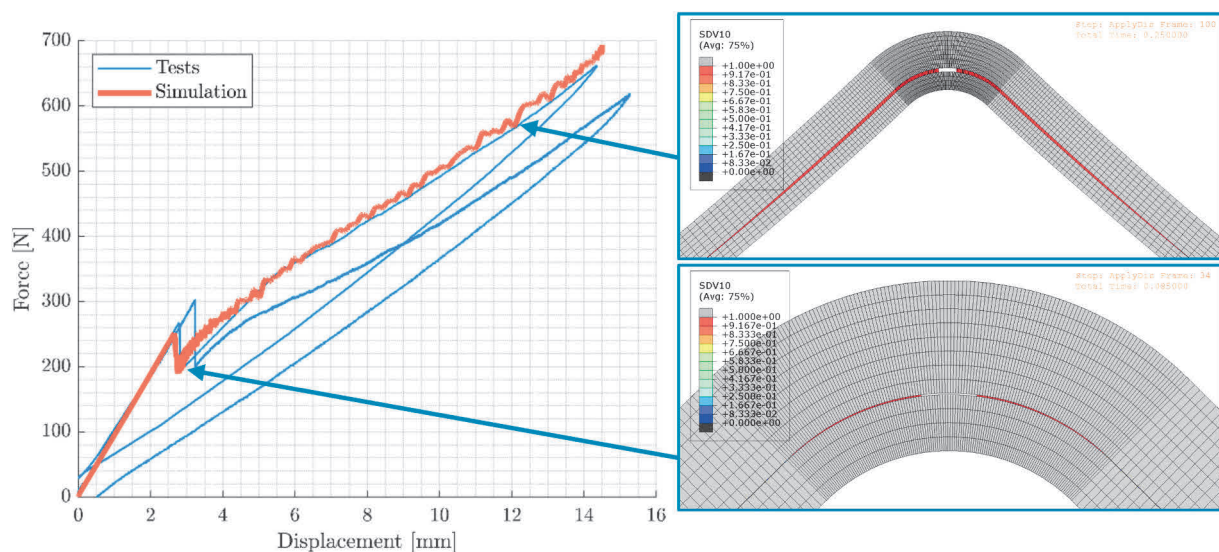


Fig. 9. Numerical vs. experimental tests on L-shaped specimen with a 2-mm defect.

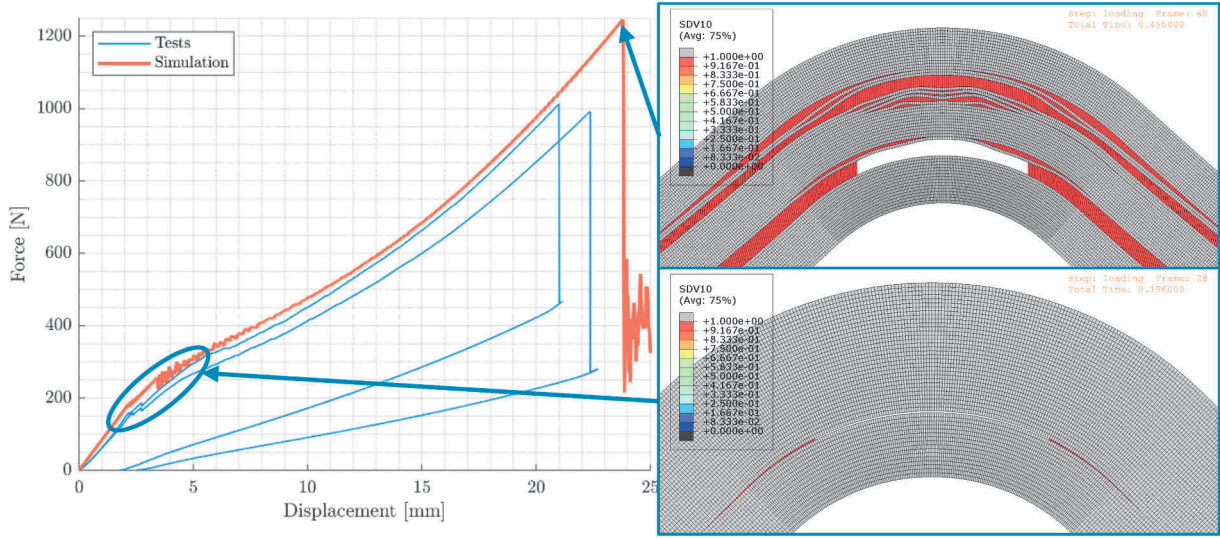


Fig. 10. Numerical vs. experimental tests on L-shaped specimen with an 8-mm defect.

An SCL analysis involves two steps: in the first step, the load on a structure is ramped up to the maximum force/displacement of the desired fatigue load block. In the second step, this load is then maintained and the damage resulting from load cycling is simulated via the constitutive law. The evolution of fatigue damage as a function of cycling, stress state, stress ratio, mode mixity, and material properties is dictated by a pre-defined fatigue damage accumulation law. The fatigue damage accumulation model used here is based on the cohesive fatigue model in [14–16], which makes use of the SCL method and an empirically derived damage accumulation function. The specific version of the law adopted in the fatigue modelling presented in this work is the CF20. Its calibration is based on the S-N curve of the material and by fitting numerical crack-growth rates to the experimental Paris curves of the material in mode I.

A fatigue damage calculation module can be easily integrated in the VUMAT cohesive subroutine previously developed for the simulation of quasi-static damage. Fig. 11-(a) illustrates a typical bi-linear cohesive zone response. The figure also illustrates the progression of damage induced by a cyclic load consisting of a constant maximum stress. During the simulation of fatigue, the traction-displacement state at a material point along the interface evolves along the path denoted as “Fatigue” in Fig. 11-(a), until the softening branch of the static cohesive law is reached, at a displacement jump λ^* . At this point, the cohesive interface is no longer able to sustain the maximum stress and the response proceeds by unstable static tearing until full separation of the cohesive interface.

At each increment in the simulation, the damage norm D is evaluated by considering both the static response, and the fatigue damage accumulation. The larger of the two values is selected as the current state damage variable. The CF20 function expresses the rate of change of the damage norm, D , with load cycles, N , as:

$$\frac{dD}{dN} = \frac{(1-D)^{-q}}{\gamma E^\beta (\beta + q + 1)} \left(\frac{\lambda}{\lambda^*} \right)^\beta \quad (3)$$

where λ/λ^* is the ratio of current cohesive displacement jump λ to the corresponding static fracture displacement λ^* . The model parameters β , E , q and γ have physical meaning in the S-N curve of the material, as illustrated in Fig. 11-(b). E is the relative endurance, i. e., the maximum stress severity for infinite life, and γ is the number of cycles corresponding to this endurance limit (Fig. 11-(b)), whereas q is the parameter responsible for shifting the Paris crack growth rates. The fatigue endurance depends on the stress ratio, and

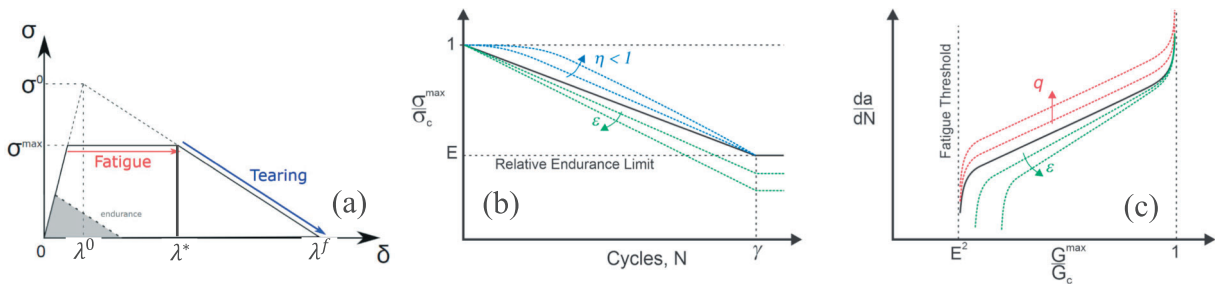


Fig. 11. Fatigue damage accumulation law: (a) single-element response in fatigue simulation, (b) effects of parameters η and ϵ in the S-N curve, and (c) effect of parameters q and ϵ in the Paris Curve. Parameters in (b) and (c) are intended as increasing in the direction of the arrows, except for η , which decreases.

the mode mixity, as in Eq. (4):

$$E = \frac{2\varepsilon_m}{\varepsilon_m + 1 + R(\varepsilon_m - 1)} \text{ where } = (1 - 0.42B)\varepsilon \quad (4)$$

where E is the relative endurance, and ε is the relative endurance for fully reversed loading ($R = -1$). The parameter β that appears in Eq. (3) is the slope of the S-N curve (Fig. 11-(b)), which is:

$$\beta = \frac{\eta \log(\gamma)}{\log(E)} \quad (5)$$

where η is the brittleness parameter, controlling the low-cycle region of the S-N curve, as shown in Fig. 11-(b). The effect of the model parameters on the S-N curve and the growth rate is illustrated in Fig. 11-(b) and Fig. 11-(c), respectively. The characterization of the fatigue damage accumulation law for the AS4/8552 unidirectional (UD) prepreg is obtained by fitting to the results of DCB fatigue propagation tests, which are presented later in Section 4.3. This characterization provides the set of material parameters needed for the simulation of the L-shaped fatigue tests with the CF20 cohesive fatigue model.

It should be noted that CF22 [16] is an updated version of the CF20 fatigue model that has been shown to be more accurate when the stress ratio and the mode mixity are both high. However, this improved model requires two additional parameters that require mode II input data. Since this data is not available for AS4/8552 and the fracture mode is dominated by mode I, the present effort uses the older CF20 model.

In the explicit time integration scheme, each time increment is associated with a number of cycles, ΔN . By discretizing the expression in Eq. (3), the current value of the fatigue damage norm can be evaluated as:

$$D_k = D_{k-1} + \frac{(1 - D_{k-1})^{-q}}{\gamma E^\beta (\beta + q + 1)} \left(\frac{\lambda_{k-1}}{\lambda_{k-1}^*} \right)^\beta \Delta N \quad (6)$$

The fictitious frequency that links simulation pseudo-time with cycle count is selected to keep ΔD small enough at each integration step to avoid the dynamic oscillations that would ensue from excessive damage increments, but large enough to avoid a waste of computational time.

The advantages of the chosen fatigue modelling framework include the fact that it unifies crack initiation and propagation within a single model, and that it is a fully local approach for the calculation of fatigue damage (i.e., damage at a given integration point does not depend on the stress and damage state of other integration points) and hence does not necessitate knowledge of the length of the process zone, which could be difficult and costly to calculate. Moreover, it does not require the establishment of a relation between the G at the crack-tip and the crack-growth rate, i.e., it does not involve the hard-coding of the Paris curve into the damage accumulation law. In most models available in the literature, e.g. [13], the measured Paris curve of the interface enters the formulation directly, as an input to the damage-evolution law. In the present approach, the damage-evolution law is built on the S-N description of the interface, and the Paris curve is an outcome of the simulation.

4.3. Characterization of the CF20 fatigue law parameters with mode I (DCB) tests

The DCB specimens used for the characterization of the fatigue damage model are laminates of unidirectional AS4-8552 prepreg plies, with a Teflon insert in the mid-interface of a $[0]_{24}$ laminate. The nominal dimensions of the specimens are listed in Table 4: initial delamination length (a_0), specimen thickness ($2h$), and width (B). The tests are carried out in displacement control, at room temperature, and on the same MTS 858 Mini Bionix II machine used to test the L-shaped specimens. The test set-up is shown in Fig. 12-(b). The test frequency is set at 5 Hz. Before testing, each specimen is opened statically just enough to visually determine the delamination length a_0 while avoiding any delamination growth.

The fatigue tests are conducted at two displacement ratios, $R = 0.1$ and 0.4 . Two specimens are tested at $R = 0.1$, at an initial load severity of 0.4 and 0.8 . Two further specimens are tested at $R = 0.4$, and both are conducted at a severity of 0.8 . For each test, the maximum imposed displacement, δ_{max} , is determined based on the quasi-static crack propagation tests performed on specimens belonging to the same manufacturing batch as those tested under fatigue loading. Static tests provide the critical force which, together with Eq. (1), is used to compute the value of δ_{max} for fatigue testing at a set initial severity. The delamination length during cycling is obtained from the specimen compliance, using compliance-calibration constants obtained from the quasi-static tests on specimens of the same batch, following the procedure described in [41]. The maximum energy release rate, $G_{I,max}$, is evaluated from the recorded maximum force and displacement with the Modified Beam Theory (MBT) data reduction in [40]. The crack-growth rates, da/dN , are then obtained by differentiating the resulting delamination-length history with respect to the cycle count. The specimens are not pre-cracked statically, prior to fatigue testing, as it is found to have very little to no effect on the Paris curves, which all align for a given R .

Table 4
DCB nominal dimensions.

a_0 [mm]	$2h$ [mm]	B [mm]
51.0	3.00	20.0

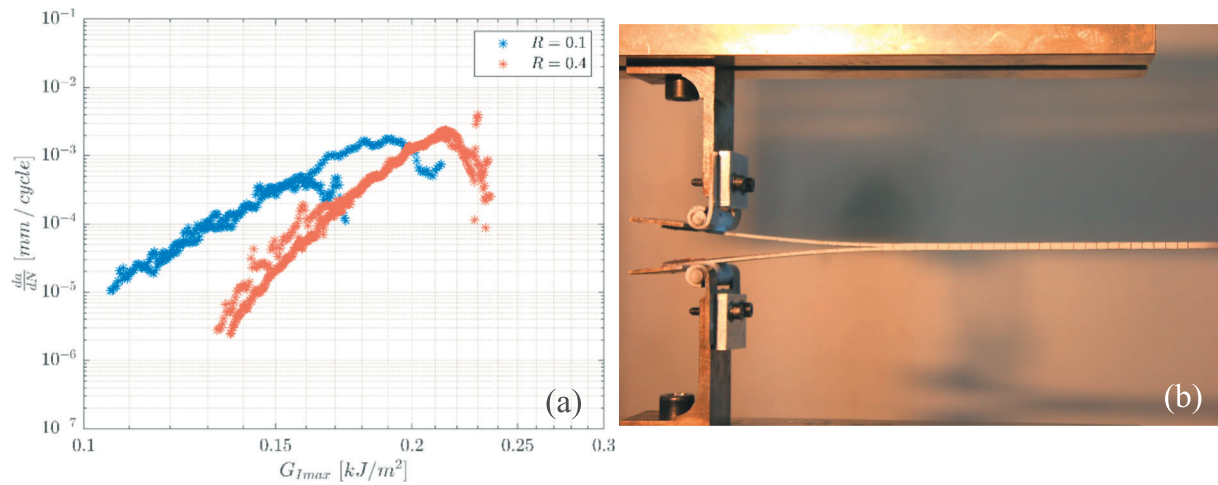


Fig. 12. (a) DCB fatigue propagation tests; (b) Test set-up.

ratio (flat R-curve). The experimental results for $R = 0.1$ and 0.4 are shown in Fig. 12-(a).

Under displacement control, a test starts at the maximum value of the ERR and, as the compliance of the specimen increases, proceeds towards lower values of the ERR. Although the propagation rates are theoretically monotonic functions of ERR, that is not the case for the experimental results in Fig. 12-(a), which initially exhibit an increase in the rate before settling along the linear curve described by the Paris law. This initial transient rise is partly the result of starting the crack in the resin-rich zone along the edge of the Teflon film, where the propagation rates are lower. The experimental scatter in the tests is well contained, and curves at the same R-ratio align well with each other.

A plane-strain model of the DCB test is constructed using six layers of CPE4R elements (four-noded, bilinear plane strain element with reduced integration) in each of the two arms of the specimen, and a layer of zero-thickness COH2D4 elements (four-noded two-dimensional cohesive element) along the interface. Two-dimensional plane strain models are used after verifying that they produce the same crack-growth rates as similarly refined three-dimensional models. The element size in the length direction of the DCB arms is 1 mm, while the region where the crack propagates is meshed with 0.10-mm-long elements, matching the discretization adopted in the curved region of the L-shaped model. Mesh convergence trials conducted with element lengths between 0.01 mm and 0.10 mm in the propagation zone show that the computed crack-growth rates remain invariant with the mesh size in this range.

The parameters of the CF20 fatigue damage accumulation law are obtained by matching the numerical results to the Paris curve corresponding to $R = 0.1$. The ε parameter is obtained by matching slopes, and the q parameter is obtained by matching the intercepts of the Paris laws with the $G_{max} = G_c$ axis. The identification is performed manually, by iteratively adjusting ε and q in successive finite element simulations of the $R = 0.1$ tests until the slope and the intercept of the numerical Paris curve match the experimental ones; since each parameter acts on a distinct feature of the predicted curve (Fig. 11), the procedure yields the final set of parameters rapidly and is straightforward to reproduce. The $R = 0.4$ data are excluded from the identification and are used as an independent verification of the identified set of parameters. A value of $\eta = 0.95$ is selected according to previous work on a similar material system [14]. The full set of fatigue model parameters used is reported in Table 5.

For verification, the parameters determined for $R = 0.1$ are also used to simulate the $R = 0.4$ tests. A comparison of the numerical and experimental results is shown in Fig. 13, where the tests results are represented with blue circles and the numerical predictions are plotted in solid orange lines. Fig. 13-(a) corresponds to $R = 0.1$, and Fig. 13-(b) corresponds to $R = 0.4$.

It can be observed that the initial rise in the propagation rates seen in the experimental results (from right to left in the figure) is not exhibited by the numerical results because the model does not account for the resin-rich region along the edge of the Teflon crack starter. Instead, the numerical rates of propagation start with a high propagation rate that quickly settles along the linear region of the Paris diagram as soon as the process zone is fully formed. Therefore, the initial transients in the experimental and numerical results are ignored.

The characterization of model parameters on the $R = 0.1$ tests also produces satisfactory results for the $R = 0.4$ tests, which validates the ability of the fatigue damage model to predict the effect of stress ratio. With the characterization of the CF20 model parameters complete, this effort now proceeds with the analysis of the fatigue crack propagation in L-shaped specimens.

Table 5
AS4/8552 parameters for CF20.

ε	η	q
0.28	0.95	7.0

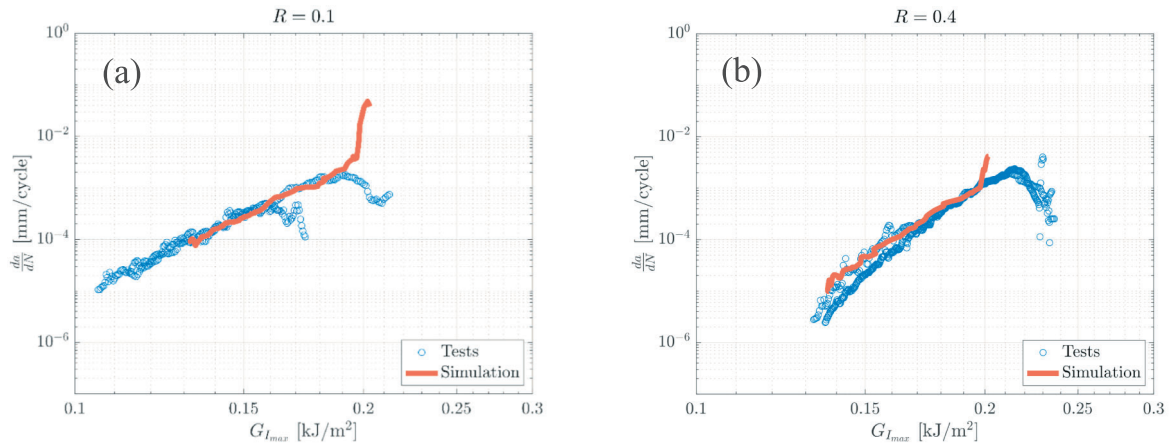


Fig. 13. DCB fatigue delamination propagation: (a) $R = 0.1$, (b) $R = 0.4$.

4.4. Simulation of the L-shaped specimen under fatigue loading

This section describes the results of simulations of the fatigue tests of the L-shaped laminated specimens with the shorter (2-mm) initial delamination. In accordance with the SCL technique described in Section 4.2, each fatigue simulation consists of ramping the applied displacement to the maximum value of the corresponding test block with a smooth amplitude, followed by a hold phase in which the effect of load cycling is accounted for through the constitutive accumulation of fatigue damage, per Eq. (6). A comparison of the numerical and experimental response of a specimen tested at a high initial severity of 0.75 is shown in Fig. 14-(a) (a fatigue FE analysis of the test at severity 0.90 cannot be conducted, since the maximum displacement of its fatigue block exceeds the critical displacement of the static response). The analysis predicts the onset of rapid crack propagation after 200 load cycles, compared with 22 cycles of the corresponding experiment. A similar comparison for two of the lower-severity tests, F.2-2-4 (severity 0.57) and F.2-2-1 (severity 0.55), is shown in Fig. 14-(b). The model predicts the onset of rapid delamination growth at about 100,000 load cycles, which lies between the two experimental results (6000 and 204,000 cycles). While the numerical prediction falls within the bounds of the experimental variance, the primary strength of the model lies in successfully capturing the phenomenological shift between the two observed cases: predicting near-instantaneous failure at high severity versus a delayed, and gradual degradation at lower severity. In the high-severity simulation, the crack growth is almost instantaneous and is arrested at a force level that is within 5 % of the experimental one (166 N against 157 N, respectively), whereas the load drop in the analysis of the lower-severity tests is not as fast. Here, the force drops more gradually over a larger number of load cycles.

Despite these discrepancies, the qualitative aspects of the response are captured in both cases. The very high sensitivity of the test elucidates the noticeable experimental scatter of the response for specimens subjected to approximately equal force levels. Three different simulations are performed by applying slightly different maximum load levels: 158 N, 160 N, and 166 N. The results shown in Fig. 14-(c) confirm that small variations in the applied load can produce very noticeable variations in the number of cycles required to activate the rapid growth propagation. A difference of just 5 % in load can move the onset of rapid propagation from 6000 up to 12,500 cycles (blue and yellow lines in Fig. 14-(c)). This result underscores the sensitivity of both the experimentation and the analysis involving delaminated bent laminates.

In Fig. 15, different snapshots of the state of damage in the FE analysis are presented: at the beginning of the fatigue step (Fig. 15-(a)), and right before and after rapid propagation (Fig. 15-(b) and Fig. 15-(c), respectively). Intact elements are shown in blue, and fully

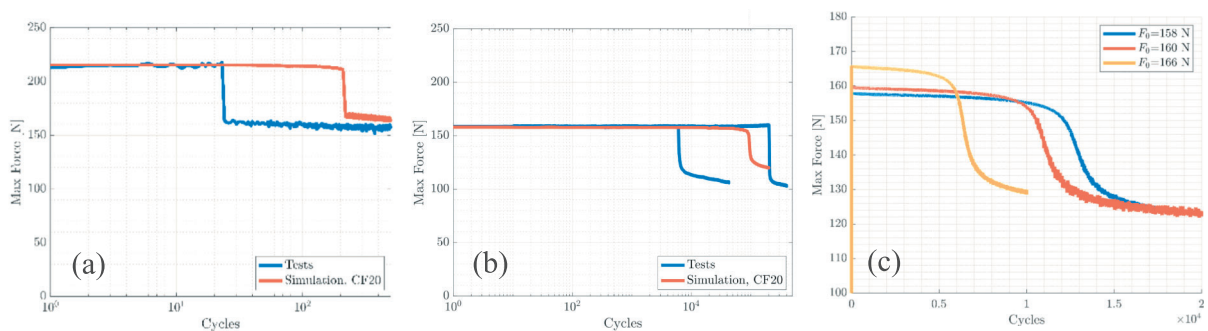


Fig. 14. FE simulations vs. experimental fatigue tests on L-shaped specimens with a 2-mm defect: (a) high-severity test case; (b) lower-severity test cases (0.57 and 0.55); (c) sensitivity of the fatigue response to applied load.

damaged elements in red. The contour of the plot shows the state variable corresponding to the damage variable d , i.e., SDV10 (the SDEG variable of the Abaqus cohesive model). The force vs. N curve in Fig. 15-(d) identifies the three main stages of the evolution of the delamination.

The results are summarized in Table 6, which reports the number of cycles required to trigger the rapid propagation registered from experiments, N_{prop} , along with the estimated crack lengths at the end of this phase, a_f , first discussed in Section 3, compared with the corresponding numerical predictions.

The values of a_f reported in Table 6 are obtained from the compliance-crack-length relation shown in Fig. 8-(a). For the experimental results, a_f is estimated by identifying the compliance corresponding to the end of the rapid propagation phase and mapping it onto the curve in Fig. 8-(a). To filter out initial compliance offsets introduced by the physical fixture, this mapping evaluates the relative stiffness degradation by multiplying the numerical baseline compliance at an initial crack length of 2 mm by the ratio of the instantaneous experimental compliance to its initial measured value. In the numerical simulations, the same curve is used to estimate the evolution of crack length from the computed compliance. The predicted crack length increases progressively during the simulation; however, the end of the rapid propagation phase can be identified as the point at which the crack growth rate reaches its maximum, beyond which propagation slows down dramatically. The experimental value is reported in Table 6, together with the corresponding numerical estimate of a_f . Numerical results confirm that crack propagation from the 2-mm defect is strongly influenced by the increase in energy release rate, which promotes crack advancement over a limited number of cycles. At high severity levels, the excess available energy drives crack propagation beyond the stable-unstable transition identified in Fig. 8-(b). Conversely, at lower severity levels, crack propagation arrests closer to this transition limit. As we have observed earlier, the 0.90 severity specimen, F.2-1-4 in Table 6, cannot be simulated under fatigue, as the maximum load block displacement of the fatigue test exceeds the critical displacement of the static model.

Both experimental and numerical results indicate that specimens tested at severities of 0.70 and above exhibit the onset of rapid delamination propagation within a few hundred cycles, whereas for severities of 0.60 and below the onset is delayed to several thousand load cycles. This one-order-of-magnitude increase in the number of cycles required to trigger rapid propagation, occurs when the severity decreases from approximately 0.70 to 0.60.

For the specimen tested at a severity of 0.62, however, the experimental onset of rapid propagation occurs after about 100 cycles, while the numerical model predicts a significantly larger number of cycles. This discrepancy can be attributed to the high sensitivity of the specimen response in this severity range, as illustrated in Fig. 14-(c), combined with the deterministic nature of the model in contrast to the stochastic distribution of properties in the actual specimens [29,30]. A further contribution to these differences may derive from the resin-rich pocket that the Teflon insert creates at the initial delamination front, analogous to the one responsible for the initial transients in the DCB data (Section 4.3), which is not represented in the model. Being a nominally identical feature of all specimens, this local region can only act systematically on the cycle count at the onset of rapid propagation; it cannot, however, account for the specimen-to-specimen variability, and it affects neither the subsequent propagation regimes nor the estimated arrest lengths. Despite these differences, the numerical model captures the overall trends observed experimentally and reproduces the evolution of the force response over a wide range of load cycles.

Tests and analyses are similarly performed on the specimens manufactured with the 8-mm initial delamination. The evolution of the maximum-force as a function of load cycles is shown in Fig. 16, along with the corresponding numerical simulation. In Fig. 16-(b) and -(c) is shown the crack tip in two distinct moments in the analysis: at the beginning, in Fig. 16-(b), and at the end in Fig. 16-(c), of the fatigue step. In contrast to the 2-mm specimens, where rapid crack growth is triggered after a finite number of cycles, the 8-mm specimens exhibit a much more gradual response, which is also matched by the fatigue FE analysis conducted. Delamination propagates slowly and stably from the edge of the Teflon insert, with the applied force decreasing progressively over several thousand cycles until reaching a plateau. This behaviour is perfectly aligned with the theoretical stability threshold identified in Section 3, where delaminations exceeding 6.3 mm are predicted to propagate stably. The fatigue FE analysis captures this stable regime quite well. As the initial crack tip ($a_0 = 8$ mm) already resides in the stable propagation region of the applied ERR curve (Fig. 8-(b)), the numerical model correctly predicts the absence of any sudden load drops. Instead, the delamination propagates slowly and stably from the edge of the Teflon insert. The simulated applied force decreases progressively over several thousand cycles, eventually reaching a plateau as the crack extends further into the structure and the driving force diminishes. The predicted plateau force lies within 4 % of the measured one (97.0 N against a mean of 93.6 N over the last 30,000 cycles of the tests).

Most importantly, the numerical analysis demonstrates remarkable quantitative and qualitative agreement with the experimental data in this stable regime, as shown by the results in Fig. 16-(a). Without the extreme sensitivity to minor load variations or material stochasticity observed in the unstable 2-mm cases, the numerical framework proves to be a robust predictive tool. It accurately captures the progressive degradation of the specimen stiffness, reinforcing the validity of the underlying cohesive zone modelling approach for predicting fatigue-driven delamination in complex composite geometries.

5. Conclusions

A novel study of delamination propagation under quasi-static and cyclic loading in pre-delaminated L-shaped specimens is conducted using specimens with two different pre-implanted delamination lengths a_0 : 2 mm and 8 mm. Quasi-static tests reveal very distinct propagation responses between the two lengths, which can be interpreted in light of the transition between unstable and stable propagation regimes at the point of maximum ERR. This transition also influences the fatigue response. The fatigue tests on specimens with the shorter delaminations are characterized by an initial stage of nucleation, in which load remains approximately constant and

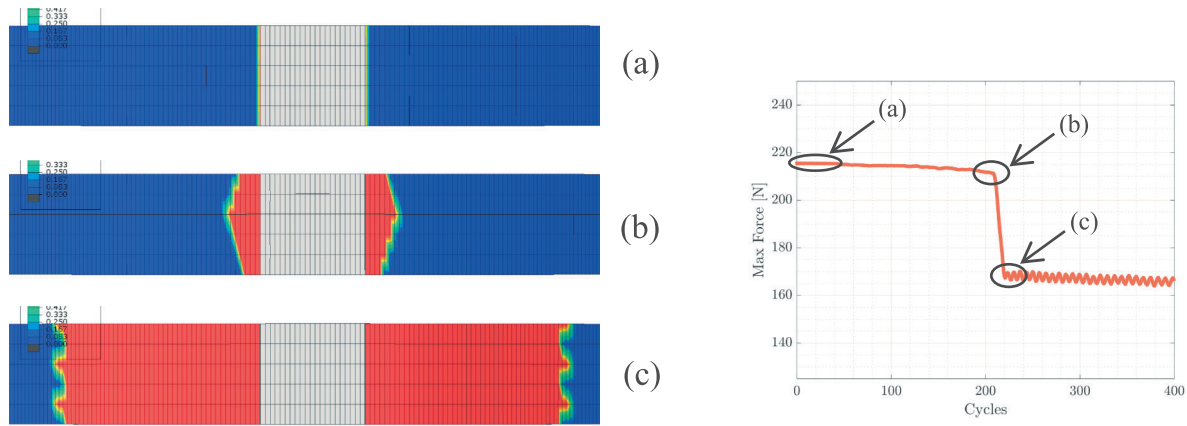


Fig. 15. Snapshots of the high-severity simulation: (a) Beginning of the fatigue step; (b) Before unstable propagation; (c) After unstable propagation; (d) The force-cycles evolution.

Table 6

Estimated crack length after initial unstable propagation for specimens with a 2-mm initial delamination.

Specimen ID		OF.2-1-4	OF.2-1-5	OF.2-2-3	OF.2-2-4	OF.2-2-1
Severity		0.90	0.75	0.62	0.57	0.55
a_f [mm]	Exp.	> 13.1	12.1	7.2	7.1	6.0
	Num.	> 13.1	7.1	6.4	6.4	6.4
$\Delta a_f, \% = 100 \frac{num - exp}{exp}$		—	−41 %	−11 %	−10 %	+7 %
N_{prop}	Exp.	41	22	95	204,000	6000
	Num.	0	200	44,000	95,000	95,000

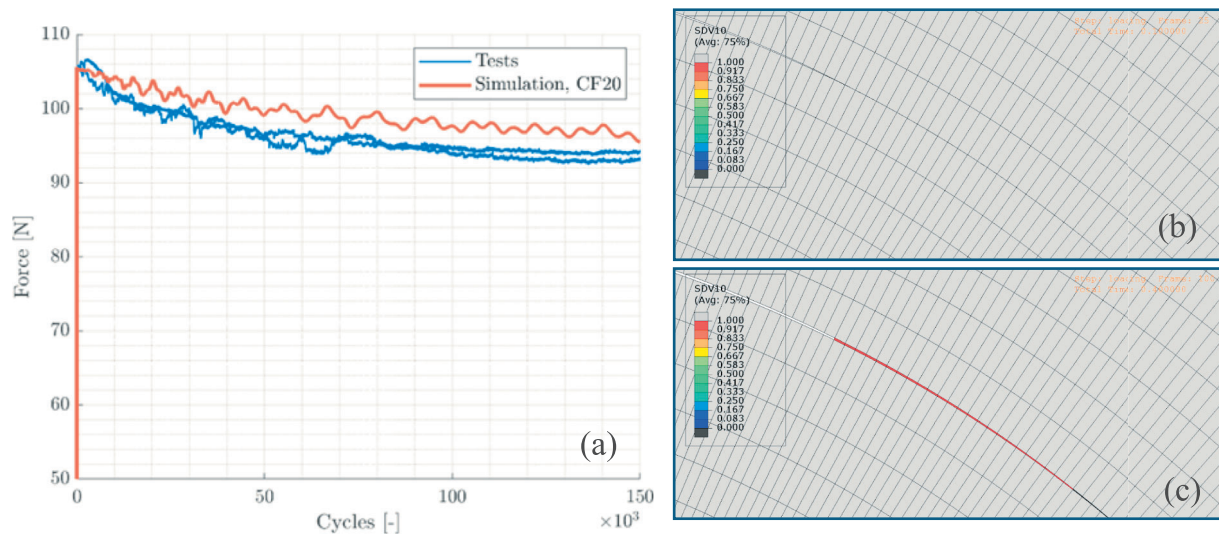


Fig. 16. (a) FE simulations vs. experimental fatigue tests on L-shaped specimens with an 8-mm defect, (b) Beginning of the fatigue step, (c) End of fatigue simulation.

with no observable crack growth. The nucleation is followed by a rapid delamination growth that reaches the end of the curved region of the specimen. After this rapid growth phase, delamination propagation stabilizes as it advances into the straight-arm regions of the specimen. While the initial load severity influences the load cycle count at which this rapid propagation phase is triggered, the same sequence of propagation regimes is observed for all the specimens with the shorter delaminations. The response of the specimens with the longer delaminations, on the other hand, is characterized by a progressive stable propagation, starting at the Teflon insert at the beginning of fatigue cycling and on until the end of the test.

The results of a semi-analytical analysis of the stability of crack growth in the specimen, based on the application of the Irwin-Kies equation elucidate these findings. In the case of the specimens with shorter delaminations, the energy available for crack opening increases with a significant gradient until a crack length of about 6.3 mm, where the ERR is more than double the starting value. This means the severity imposed at the beginning of the tests increases rapidly as soon as the crack advances, which promotes rapid crack propagation. Experimental results indicate that crack lengths at the end of this phase of rapid crack propagation are located in the vicinity of the predicted unstable-to-stable transition. This agreement indicates that this stability analysis provides a valuable prediction of the crack length at which unstable propagation transitions to stable growth. At this point, the severity decreases with further crack growth, and delamination is predicted to proceed in a stable manner, as confirmed by the experimental observations.

The three phases of the fatigue response observed in the tests with the shorter delaminations are qualitatively reproduced by FE analyses with satisfactory quantitative agreement, notwithstanding the large experimental scatter commonly reported for fatigue propagation tests on similar specimens. The application of an explicit time integration scheme enables the robust modelling of different propagation regimes with a satisfactory level of reliability, in both quasi-static and fatigue analyses. The results indicate that the proposed approach can predict static and fatigue delamination growth in configurations characterized by pronounced variations of the energy release rate with crack advancement, such as the L-shaped geometry investigated here, capturing the onset of rapid propagation, its arrest, and the subsequent stable growth. The generalization of these conclusions to other configurations will require further validation. Such conditions may lead to abrupt damage growth, with a complex interaction of static and fatigue-driven delamination phenomena, relevant to bent (or curved) laminated structures.

With regard to the practical implications for the design and damage tolerance assessment, the results support a two-step use of the proposed methodology. First, the semi-analytical evaluation of the energy release rate as a function of crack length (Section 3) provides a computationally inexpensive screening tool to map the stability of delamination growth in a structural detail: regions in which $dG/da < 0$ are candidates for a slow-growth approach, whereas configurations operating on the rising branch of the G–a curve are prone to rapid propagation and are more suited to a no-growth substantiation. Second, within the stable regime, the cohesive fatigue analysis provides the predictions of growth onset and propagation needed to demonstrate that growth is slow, stable and predictable — as required for the slow-growth substantiation of Category 2 damage in [1] — and hence to define critical damage sizes and support the definition of inspection intervals.

Some limitations of the present study, and the corresponding directions for future work, should be noted. The experimental campaign relies on a limited number of specimens per configuration, and hence the statistical distribution of the fatigue response could not be characterized; the numerical framework is deterministic, so that the stochastic variability inherent to interlaminar properties is not reproduced. Moreover, the validation is limited to a single specimen configuration, the unidirectional L-shaped laminates, which are loaded predominantly in mode I with a limited mode II contribution. Finally, the CF20 model was adopted because mode II fatigue data are not available for AS4/8552; the application of its evolution, CF22, whose additional parameters are calibrated with mode II fatigue data, is expected to further improve the calibration of the interface law and the resulting predictions, particularly for load cases with a higher mode II contribution.

CRedit authorship contribution statement

Maikel Khella: Writing – original draft, Investigation. **Pietro Ballarin:** Writing – review & editing, Investigation. **Carlos G. Dávila:** Writing – review & editing, Supervision. **Alessandro Airoldi:** Writing – review & editing, Supervision, Methodology, Conceptualization.

Declaration of competing interest

The authors declare that they have no known competing financial interests or personal relationships that could have appeared to influence the work reported in this paper.

Data availability

Data will be made available on request.

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