Prediction methodology for fatigue crack growth behaviour in Fibre Metal Laminates subjected to tension and pin loading

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Abstract

Fibre Metal Laminates (FMLs) are a hybrid metal-composite laminate technology known for their superior resistance to fatigue crack growth compared to monolithic metals. This crack growth behaviour has been the subject of many studies, resulting in numerous empirical and analytical models to describe the complex damage growth phenomenon in the material. This study builds upon the analytical Alderliesten crack growth prediction methodology for FMLs, extending it from a tension loaded plate to a case of a combined tension-pin loaded plate. This new loading case is a more representative case to utilise for predicting fatigue crack growth behaviour in mechanically fastened joints. Development of the model extension and validation through experimental testing are detailed within this paper.

Keywords: pin loading, fibre metal laminates, crack growth behaviour

1. Introduction

Fibre Metal Laminates (FMLs) are a material technology known for their superior fatigue crack growth behaviour. This favourable behaviour is a result of the fibre bridging mechanism whereby the intact fibre layers provide an alternative load path around the cracked metal layers, reducing stress in front of the crack tip (see Fig. 1).

Although the basic concept of fibre bridging is simple to understand, it proved to be a complex phenomenon to capture effectively in crack growth prediction models for FMLs. Early attempts at predicting fatigue crack growth took a phenomenological approach, treating an FML as a bulk material and developing empirical $\beta$ correction factors to represent the contribution of the fibre bridging mechanism. These $\beta$ corrections were then used to correct the standard stress intensity factor solutions used in the Linear Elastic Fracture Mechanics approaches for crack growth prediction in monolithic materials [1–3]. Additional phenomenological approaches based on treating FMLs as a bulk material include the compliance method of Takamatsu [4], bridging stress linearization approach of Cox [5], and the...
equivalent crack length approach of Guo and Wu [6]. Although these models achieved some limited success, the bridging mechanism could not be adequately captured with this bulk material approach [7].

Greater success was achieved by embracing the composite nature of FMLs and attempting to analytically describe the interplay between the metal and fibre layers. Marissen [8] investigated the influence of bridging stress on the growth of the interface delamination between cracked metal layers and intact fibre layers. The opening of the crack in the metal layers is dependent on the compliance, and thus size, of this delaminated region. Alderliesten [9, 10] further built on this work by formulating an analytical fracture mechanics model that captured the load redistribution of the fibre bridging mechanism by enforcing compatibility between the crack opening displacement in the metal layers and elongation of the delaminated region of the fibre layers. With the bridging stress determined, growth of the interface delamination under the driving force of the bridging stress could be predicted, and growth of the crack through superposition of the far-field and bridging stress intensity factors in the metal could be achieved. This analytical approach, referred to in this paper as the Alderliesten model, has been the backbone of continued effort in extending crack growth prediction capabilities in FMLs with extensions to account for residual strength [11], variable amplitude loading [12–14], generalized laminate configurations [15, 16], and more recently multiple site damage [17, 18].

It is worth noting that mechanically fastened joints are potentially vulnerable structures in consequence of secondary bending, stress concentration at fastener holes and pin bearing effects as load transfers from one substance to another via the joint. The structural behaviour of mechanically fastened FML joints has therefore drawn particular attention. In open literature, the neutral line model developed by Schijve [19] has been extended by de Rijk for calculating the load transfer and secondary bending stresses in FML joints [20]. The progressive damage behaviour in pin loaded FMLs has been investigated by Frizzell et al. [21, 22] and the bearing strength of FMLs has also been extensively studied [23–25].

Apart from the static behaviour of FML joints, another issue associated with mechani-
cally fastened FML joints is the fatigue crack growth in the metal layers and the delamination propagation at metal-composite interfaces under fatigue loading. Even though extensive analytical models have been developed for predicting the crack growth behaviour in flat FML panels subjected to tensile loading [9, 10, 14, 15], the influence of pin loading on the crack growth behaviour in FMLs has not been fully studied. There is a risk that multiple cracks are present simultaneously in the critical row of an FML joint, which is crucial to examine in light of the introduction of Limit of Validity (LOV) to the airworthiness regulations that defines a fatigue life free of Widespread Fatigue Damage [18, 26, 27]. The analysis of pin loading effects on the crack growth behaviour in FMLs becomes indispensable for the analysis of MSD crack growth behaviour in FML joints.

This paper aims to develop an analytical model capable of predicting the growth behaviour of an isolated crack in a mechanically fastened FML joint where the pin loading effects are present. This model is developed with the intention to further incorporate it into an analysis frame which can eventually analyse the MSD growth behaviour in FML joints. The development of this model is based on the findings of an experimental investigation of pin loading effects on the crack growth behaviour in FMLs [28] and the success of an analytical model by Alderliesten for analysing the damage growth in FMLs subjected to pure far-field loading. Firstly the test procedure and test results are briefly summarized in Section 2, and then the model development is detailed in Section 3. The analytical model will be validated against the test data in Section 4.

2. Remarks regarding test results

The principle of superposition is normally applied to calculate the stress intensity factor for a crack in a metallic panel subjected to tension-pin loading by splitting the loading case into simpler loading cases and summing the stress intensity factors for simpler split loading cases together [29]. The crack growth mechanism in FMLs, however, differs from that of monolithic metallic panels because of the fibre bridging. The bridging mechanism is accounted for using the principle of superposition in the Alderliesten model. The total stress intensity factor at the crack tip in an FML is a superposition of the far-field stress intensity factor and the bridging stress intensity factor [9, 10]. The calculation of the stress intensity factor, $K_{\text{joint}}$, at the crack tip in an FML subjected to tension-pin loading therefore have more complications.

An experimental investigation into the effects of pin loading on the fatigue crack growth behaviour in FMLs has been carried out by the current authors [28]. These experiments were conducted to achieve two objectives. The first objective was to observe the fatigue damage mechanism in FMLs subjected to tension-pin loading. The second objective was to verify that the principle of superposition can be used to calculate the stress intensity factor, $K_{\text{joint}}$, at a crack tip in an FML plate subjected to tension-pin loading. This calculation can be conducted by decomposing the complex tension-pin loading acting on a cracked FML into simpler loading states for which total stress intensity factors can be determined, superposing the calculated total stress intensity factors. The calculation of the total stress...
intensity factor at a crack tip in an FML plate subjected to a simpler loading case follows the same basic approach as the Alderliesten model [9, 10].

The joint configuration chosen for this study is a symmetric double shear joint as illustrated in Fig. 2. This configuration avoids secondary bending effects in the joint that were not taken into account in the present model development. The test configuration thus permits a more accurate validation of the performance of the model.

Table 1: Test matrix [28]

<table>
<thead>
<tr>
<th>Tested joint type</th>
<th>$P_{\text{max}}$ [kN]</th>
<th>$R$</th>
<th>$f$ [Hz]</th>
<th>Number of coupons</th>
</tr>
</thead>
<tbody>
<tr>
<td>Type 1 joint</td>
<td>10</td>
<td>0.05</td>
<td>10</td>
<td>2</td>
</tr>
<tr>
<td>Type 2 joint</td>
<td>20</td>
<td>0.05</td>
<td>10</td>
<td>2</td>
</tr>
</tbody>
</table>

The basic joint configuration tested comprised of outer plates of Glare3-3/2-0.4 and an inner plate of Glare3-7/6-0.4 (Fig. 2(a)). Two variations of this configuration were adopted:
Type 2 joint Type 1 joint Bypass loading

Figure 3: Relation of the loading cases for the two types of joints

A type with one pin (Fig. 2(b)), and another type with two pins (Fig. 2(c)). Fatigue testing on these joints was carried out under constant amplitude fatigue loading conditions. The maximum applied load, $P_{\text{max}}$, stress ratio, $R$, and testing frequency, $f$, for the two types of joint are summarized in Table 1. A computer controlled 60 kN servo-hydraulic fatigue testing machine with a 100 kN load cell (model 647.10A) was used to conduct the fatigue testing [28].

In the joint with one pin illustrated in Fig. 2(b), the load transfers from the middle plate to the specimens through the only pin. The crack in the specimen therefore subjects to pin loading and far-field loading. In addition to the loading cases for the cracked specimen in this type of joint, the additional pin in another joint shown in Fig. 2(c) introduces bypass loading to the crack. A small degree of plastic deformation at the fastener holes of both pins was found following fatigue testing. This deformation was due to the high pin bearing load in Type 2 joint. The plastic deformation and fatigue crack growth from the additional pin hole in the specimens in Type 2 joint indicate an approximately equal load transfer by the two pins [28, 30].

A comparison of the loading cases for the cracks of interest in each of the specimens in the two types of joints is given in Fig. 3. The maximum bypass loading $F_{\text{bypass}}$ and pin loading $F_{\text{pin}}$ are equal to 5 kN.

The stress intensity factor ranges for the loading cases illustrated in Fig. 3 have been calculated based on the measured crack growth rates of the cracks in the two joint types. The calculation was accomplished using the empirical Paris relation for the metal sheet used and the stress intensity factor ranges have been plotted versus crack length, $a$, in Fig. 4(a). For the given bypass load, the stress intensity factor ranges for only a small crack increment were calculated with the Alderliesten model [9, 10]. The stable crack growth behaviour of FMLs was then exploited to extrapolate the data for a larger crack length range.

The superposition data in Fig. 4(b) is obtained by superposing the stress intensity factor range for the loading case of the specimen in Type 1 joint upon the stress intensity factor...
range for the bypass loading. Good correlation between the superposed results and results for Type 2 joint in Fig. 4(b) can be observed. Therefore, the superposition relation of the stress intensity factor ranges for the loading cases illustrated in Fig. 3 is also valid, which is expressed by the following equation:

$$\Delta K_{\text{pin+bypass}} = \Delta K_{\text{pin}} + \Delta K_{\text{bypass}}$$  \hspace{1cm} (1)

Since the stress ratios for all the tested configurations are the same and the maximum stress intensity factor and the stress intensity factor range have a linear relation, Eq. 1 can be reformulated, in terms of maximum stress intensity factors, as follows:

$$K_{\text{pin+bypass}} = K_{\text{pin}} + K_{\text{bypass}}$$  \hspace{1cm} (2)

The test data verifies that the total stress intensity factors for each of the simpler loading cases acting on a cracked FML can be superposed to get the stress intensity factor of the crack in the FML subjected to a complexly compound loading system comprising the simpler loading cases. Then the key to the problem is how to derive the total stress intensity factor for each simpler loading case acting on a cracked FML, i.e., calculation of $K_{\text{pin}}$ and $K_{\text{bypass}}$ in Eq. 2.

3. Model development

3.1. State of the art in FML crack growth prediction

Fatigue crack growth of the metal layers in an FML is accompanied by delamination propagation at the metal/composite interfaces. The fibres in the wake of fatigue cracks within the metal layers remain intact and bridge the crack opening. This bridging mechanism
reduces the stress transferred at the crack tip, which could improve crack growth resistance in the metal layers. Meanwhile, the bridging mechanism introduces cyclic shear stresses at the metal/composites interfaces, which could result in delamination growth [9, 10].

Alderliesten has developed an analytical model to predict the coupled crack growth and delamination propagation in FMLs under far-field applied fatigue loading [9, 10]. Both the crack growth and delamination growth are characterized using classical LEFM approaches. A summary of the methodology of his model is provided in this work. The reader is referred to [9, 10] for a more detailed review.

In the context of LEFM, the stress state at the crack tip in the metal layers is characterized by stress intensity factors. The total stress intensity factor, $K_{\text{total}}$, at the crack tip in the metal layers in FMLs for a given load can be decomposed into two terms using superposition:

$$K_{\text{total}} = K_{ff} + K_{br}$$

(3)

where $K_{ff}$ is due to the stresses in the metal layers resulting from the far-field applied load and $K_{br}$ is due to the bridging mechanism.

The strain energy release rate, $G$, is employed to characterize the complex stress state at a metal/composites interface, which is given as:

$$G = \frac{n_f t_f}{2 j E_f} \left( \frac{n_m t_m E_m}{n_m t_m E_m + n_f t_f E_f} \right) (S_f + S_{br}(x))^2$$

(4)

For the metallic material used, $K_{\text{total}}$ and an empirical Paris relation can be employed to calculate the crack extension in the metal layers. Similarly, $G$ and an empirical Paris relation for the resistance of the delamination growth can be employed to calculate the delamination propagation. However, the calculation of $K_{br}$ and $G$ can only be implemented once the bridging stress distribution, $S_{br}(x)$, is determined [9, 10].

Alderliesten calculates $S_{br}(x)$ by employing the displacement compatibility between the metal layers and fibre layers over the delaminated length, which is expressed in Eq. 5. The crack opening displacement in the metal layers which comprises a crack opening term $v_{ff}(x)$ due to applied load and a crack closing term $v_{br}(x)$ due to bridging mechanism should be equal to the elongation of the delaminated fibre layers $\delta_f(x)$ and deformation due to shear $\delta_{pp}(x)$.

$$v_{ff}(x) - v_{br}(x) = \delta_f(x) + \delta_{pp}(x)$$

(5)

Each term in Eq. 5 varies along the delamination front. Alderliesten divides the delamination shape into bar elements. At $x$ of each bar element location, Eq. 5 needs to be implemented. A numerical matrix therefore has to be applied to simultaneously solve for the displacement compatibility at every discretized element along the delamination shape.

3.2. Prediction model incorporates pin loading effects

The Alderliesten model has already provided the solution for calculating $K_{\text{bypass}}$ since the bypass loading can be treated as far-field load. Based on the experimental findings, it can be argued that the overall stress intensity factor $K_{\text{pin}}$ for a crack in an FML subjected to both pin loading and far-field loading, a non-symmetric loading case, can be estimated by
Figure 5: Estimation of total stress intensity factor for asymmetric pin loading case in FMLs

decomposing the loading cases, calculating the total stress intensity factor for each loading case, and summing the total stress intensity factor of each loading case as illustrated in Fig. 5.

Therefore the estimation of \( K_{\text{pin}} \) can be expressed by the following:

\[
K_{\text{pin}} = 0.5(K_{\text{pin,bearing}} + K_{\text{pin,ff}})
\]  

(6)

Again \( K_{\text{pin,ff}} \) can be calculated with the Alderliesten model. At this point, there is only one unknown variable \( K_{\text{pin,bearing}} \). It is noteworthy that the calculation of \( K_{\text{pin,bearing}} \) should also involve calculating the stress intensity factor resulting from the bridging mechanism. In view of the computational cost when individually implementing displacement compatibility for each simpler loading case of a given crack and delamination configuration, a succinct approach incorporating the analysis of pin loading effects needs to be developed for FMLs.

Analogous to the approach adopted by Alderliesten, the overall stress intensity factor \( K_{\text{joint}} \) for a cracked FML subjected a tension-pin loading system as illustrated on the left in Fig. 3 can be decomposed into two terms using the principle of superposition. As expressed in Eq. 7, \( K_{\text{app}} \) refers to the stress intensity factor due to the applied loads such as the far-field applied load, pin bearing load and the bypass load. \( K_{\text{br}} \) refers to the stress intensity factor due to the bridging mechanism. When calculating the bridging stress distribution by implementing displacement compatibility, the crack opening displacement should be comprise of all of the opening terms resulting from the applied loads.

\[
K_{\text{joint}} = K_{\text{app}} + K_{\text{br}}
\]  

(7)
The superposition relation for the complex loading cases illustrated in Fig. 3 and Fig. 5 can be still used. $K_{\text{app}}$ can also be decomposed into terms due to the corresponding simpler loading cases, as expressed in Eq. 8. $K_{\text{pin,bearing}}^*$ denotes the stress intensity factor in the metal layers resulting from a pair of pin loads, $F_{\text{pin}}$, acting on the crack flanks and $K_{\text{pin,ff}}^*$ denotes the stress intensity factor in the metal layers due to the far-field load, $F_{\text{pin}}$, see Fig. 5. $K_{\text{bypass}}^*$ refers to the stress intensity factor due to far-field bypass loading which is illustrated in Fig. 3. The calculation of $K_{\text{pin,ff}}^*$ and $K_{\text{bypass}}^*$ can be carried out according to the approach adopted by Alderliesten [9, 10].

$$K_{\text{app}} = 0.5 \cdot K_{\text{pin,bearing}}^* + 0.5 \cdot K_{\text{pin,ff}}^* + K_{\text{bypass}}^*$$ (8)

3.3. $K_{\text{pin,bearing}}^*$ due to pin loading

The load applied by a fastener to the jointed panels occurs through a pin bearing scenario. Since the metal layers in FMLs are thicker and stiffer than fibre layers, it is assumed for this work that the metal layers bear all of the load applied by the fastener and that the cut fibres along the fastener hole do not carry any load. The intact fibres in the wake of the fatigue cracks in the metal layers restrict the crack opening induced by the pin loading.

![Figure 6: A pair of point loads acting on crack flanks](image)

The pin loads applied by the fastener to the hole edges in the metal layers are regarded as point loads in this paper [31]. Fig. 6 illustrates a pair of point loads acting on the edges of a crack in a metal panel with $P$ as a point load per unit thickness. For a given pin load in the laminate, $F_{\text{pin}}$, the value of the point load for a metal layer can be calculated with the following equation:

$$P = \frac{F_{\text{pin}}}{n_m t_m}$$ (9)

where $n_m$ is the number of metal layers and $t_m$ is the thickness of each metal layer.

The stress intensity factors for the two crack tips subjected to the loading case illustrated in Fig. 6 can be written as [31]:

$$K_a = \frac{P}{\sqrt{\pi a}} \frac{\sqrt{a^2 - b^2}}{a - b}$$ (10)
\[ K_{-a} = \frac{P}{\sqrt{\pi a}} \frac{\sqrt{a^2 - b^2}}{a + b} \]  

(11)

and the corresponding crack opening is given as:

\[ 2 \cdot v(x, 0) = \frac{4P}{\pi E \cosh^{-1} \frac{a^2 - bx}{a|x - b|}} \]  

(12)

For the case in which the point loads act at the centre of the crack, Eq. 10, Eq. 11 and Eq. 12 can be rewritten concisely with \( b = 0 \). The stress intensity factors for the two crack tips are equal and the crack opening contour is symmetric with respect to the centre of the crack.

Then \( K_{\text{pin,bearing}}^* \) due to a pair of bearing loads can be calculated by substituting Eq. 9 and \( b = 0 \) into either Eq. 10 or Eq. 11:

\[ K_{\text{pin,bearing}}^* = \frac{F_{\text{pin}}}{n_m t_m \sqrt{\pi a}} \]  

(13)

and the corresponding crack opening displacement is given as:

\[ 2 \cdot v_{\text{pin,bearing}}(x) = \frac{4P}{\pi E \cosh^{-1} \frac{a}{x}} \]  

(14)

3.4. Implementation of displacement compatibility

In view of the computational cost when individually implementing displacement compatibility for each loading case, the total crack opening displacement resulting from different loading cases can be calculated and the displacement compatibility between cracked metal layers and bridging fibres over delaminated length can only be implemented once.

The displacement compatibility is expressed by Eq. 15. The sum of \( 0.5v_{\text{pin,bearing}}(x) \) and \( 0.5v_{\text{pin,ff}}(x) \) is an equivalent crack opening as a result of the asymmetric loading illustrated in Fig. 5 in the metal layers. \( v_{\text{bypass}}(x) \) denotes the crack opening due to bypass loading. The crack opening in the metal layers as a result of the applied loads and crack closing, \( v_{\text{br}} \), due to the bridging stress distribution in the bridging fibres must equal the elongation of the fibre layers over the delaminated length, \( \delta_f(x) \), plus the deformation in the fibre layers due to shear at the delamination front \( \delta_{pp}(x) \).

\[ 0.5v_{\text{pin,bearing}}(x) + 0.5v_{\text{pin,ff}}(x) + v_{\text{bypass}}(x) - v_{\text{br}}(x) = \delta_f(x) + \delta_{pp}(x) \]  

(15)

\( v_{\text{pin,ff}}(x) \) and \( v_{\text{bypass}}(x) \) can be derived by treating the loads as far-field loads. The stresses in the metal layers resulting from these far-field loads can be calculated using Classic Laminate Theory [32, 33]. For the detailed calculation of these variables in Eq. 15, please refer to the work of Alderliesten [9, 10].

Solving for the displacement compatibility expressed in Eq. 15 simultaneously for all of the bar elements provides the bridging stress distribution, \( S_{\text{br}}(x) \). Once this bridging stress distribution is known, \( K_{\text{br}} \) and the strain energy release rate \( G \) along the delamination front can be calculated [9, 10].
3.5. Crack growth model and delamination growth model

Once the overall stress intensity factor at the crack tip for a crack subjected to pin loading and far-field loading is estimated, the crack growth rate can be calculated using an empirical Paris relation for the metallic material employed in an FML.

\[
\frac{da}{dN} = C_{cg}(\Delta K_{eff})^{n_{cg}}
\] (16)

where \( C_{cg} \) and \( n_{cg} \) are Paris constants. For the tested Glare, \( C_{cg} = 2.17 \times 10^{-12} \) and \( n_{cg} = 2.94 \) [9, 10].

Knowing the bridging stress distribution \( S_{br}(x) \) along the delamination shape, the strain energy release rate at the composite/metal interface can be calculated with Eq. 4. And the delamination growth rate at each discretized element can be similarly calculated using a Paris relation:

\[
\frac{db}{dN} = C_{d}(\sqrt{G_{max}} - \sqrt{G_{min}})^{n_{d}}
\] (17)

where \( C_{d} \) and \( n_{d} \) are Paris constants. For Glare, \( C_{d} = 0.05 \) and \( n_{d} = 7.5 \) [9, 10].

4. Validation and discussion

This model is validated through comparing the predicted crack growth results with the measurements of the two tested joint types. The crack growth rates are plotted against the crack length in Fig. 7 for both loading cases. It can be observed that the prediction results correlate with the measurements very well.

As can be seen in Fig. 7, the crack growth rates of both loading cases decrease dramatically with increasing crack length in the vicinity of the fastener hole. This is because the driving force induced by pin loading decreases exponentially as the crack length increases (Eq. 13).
Without bypass loading, the crack growth driven by pin loading stops around 10 mm. For another loading case, the crack growth becomes very stable when the crack tip is away from the fastener hole, which is consistent with the stable crack growth results of FMLs under uniform far-field loading [9, 10].

The delamination shape of the specimen in Type 1 joint is obtained by etching the outer aluminium layers away following fatigue testing and is shown in Fig. 8(a). Minimal delamination is noted at the hole perimeter due to the small scale plastic deformation of the metal layers resulting from the high bearing load. As shown in Fig. 8(a), the delamination shape inclines to the direction of pin loading and so does the crack path. This non-symmetry, compared to a Mode I cracking scenario in an FML under uniform far-field loading, is neglected in this paper. The crack length, $a$, and the delamination length, $b$, are measured as depicted in Fig. 8(a).

![Delamination shape after etching](image)

(a) Delamination shape after etching

![Predicted delamination shape](image)

(b) Predicted delamination shape

Figure 8: Delamination comparison for the specimen in Type 1 joint

In order to get a closed analytical solution in the prediction model, the non-symmetric loading of a pin load and its corresponding far-field load is treated as half of the superposition of two symmetric loading cases, which is illustrated in Fig. 5. Therefore the prediction
model analyses a Mode I crack growth with symmetric delamination shapes with respect to the crack path. The predicted delamination length is plotted against the measured delamination length along the crack flank in Fig. 8(b). The overall prediction correlates very well with the measurements with the exception of the delamination length in the vicinity of the pin hole which was over-predicted. Based on Eq. 15 for displacement compatibility, the delaminated length of the bridging fibres is of significance. Consequently, the analytical model in this paper predicts a symmetric delamination shape which is equivalent to the measured delamination shape in terms of the bridging mechanism.

5. Conclusion

An analytical model incorporating the analysis of the effects that pin loading has on the fatigue crack growth behaviour in FMLs has been presented and validated in this paper. It has been demonstrated that the total stress intensity factor in the metal layers of an FML due to external applied loading cases can be calculated by summing the stress intensity factors for each of the simpler loading cases together. Furthermore, the total crack opening displacement due to external loading cases can be derived in the same manner. The displacement compatibility between the crack opening and the deformation of the delaminated fibre layer can then be used to calculate the load transferred between the cracked metal layers and the intact bridging fibres and thus the stress intensity factor induced by the bridging mechanism.

Despite the fact that the pin loading in the FML joints results in a non-symmetric loading scenario for the crack, the model using the superposition of the two symmetric loading cases successfully estimates the driving force for the crack. The model captures the rapid crack growth in the vicinity of the fastener hole. The pin loading effects on the crack growth behaviour diminish as the crack tip grows away from the location of pin loading, which is also successfully predicted by the analytical model.

The non-symmetric loading scenario slightly deviates the crack growth from the transverse direction perpendicular to the far-field loading direction. Non-symmetric delamination shapes with respect to the crack flank also develop. These effects are neglected in the prediction model. However, the model still provides a reasonable prediction of an equivalent delamination shape in terms of the bridging mechanism.

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References


