Similarity laws of the fiber-matrix interface crack in polymer composites

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ABSTRACT

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1. Introduction

One of the most promising developments in Fiber Reinforced Polymer Composites (FRPCs) for advanced structural applications is currently represented by *thin-ply* laminates [1]. Constituted by extremely thin plies, with $t_{90°}$ as small as just $\sim 4-5$ fiber diameters, this family of laminates is characterized by its damage tolerance, in particular the capability of delaying to higher strains and even suppressing the onset and propagation of transverse cracks [2]. The recent experimental assessment of transverse cracks suppression in *thin-ply* laminates [3, 4, 5] validates the existence of a *ply-thickness* effect [5] at scales $10x$ smaller than those at which it was originally observed at the end of the 1970's [6]. Onset of transverse cracks coincides at the microscopic level with the formation of fiber/matrix interface cracks [7], or debonds. After the inter-fiber stress [8] and strain concentration [9] causes the matrix to fail at or close the fiber interface, debonds grow along the fiber arc direction until a maximum or critical size is reached. If the applied load is increased, debonds move into the matrix or "kink" out of the fiber/matrix interface [10, 11]. Coalescence of debonds then occurs, which corresponds macroscopically to through-the-thickness transverse crack propagation [10, 12]. Finally, propagation through the specimen width occurs [10].
Given that *thin-plies*, as previously noted, can reach nowadays thicknesses of just $\sim 4-5$ fiber diameters, the characteristic size of the ply, i.e. the thickness $t_{90°}$, is now comparable in magnitude to the characteristic size of debonds, i.e. the fiber diameter $2R_f$, such that $t_{90°}/(2R_f) \sim \mathcal{O}(1)$. This has motivated in recent years a renewed interest in debond growth modeling [13, 14, 15, 16]. Since the elastic solution to the interface crack problem implies an oscillating solution at the crack tip [17] in the *open* case (crack faces not in contact), Stress Intensify Factors (SIFs) are not defined and debond growth characterization has focused on the determination of Mode I, Mode II and total Energy Release Rate (ERR). Many authors have reported their results in normalized form [11, 18, 19], by defining a reference ERR G_0. The definition of such reference ERR would be useful to establish similarity laws and thus to allow comparisons between different material systems, scales, loads and microstructural arrangement. However, no agreement can be found in the literature on the very definition of G_0 and expressions vary between authors. Furthermore, no clear derivation of G_0 has been proposed. In this brief contribution, we provide a derivation of G_0 based on arguments of dimensional analysis, material homogenization and fracture mechanics; we then apply the derived expression of reference ERR to the analysis of debond growth in Representative Volume Elements (RVEs) of UD composites and cross-ply laminates.

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2. Dimensional analysis

We first recall that the Energy Release Rate G has units of energy E per unit area:

$$[G] = \frac{E}{L^2}, \tag{1}$$

where L stands for unit of length. By algebraic manipulation of Equation 1 we can write the units of ERR as

$$\frac{E}{L^2} = \frac{F \cdot L}{L^2} = \frac{F}{L^2}\frac{L}{L}L, \tag{2}$$

where F stands for unit of force. We recognize that, in Equation 2

$$\frac{F}{L^2} = [\sigma] \qquad \frac{L}{L} = [\varepsilon], \tag{3}$$

where σ and ε are respectively stress and strain. The reference Energy Release Rate is thus dimensionally equivalent to a reference stress σ_{ref} times a reference strain ε_{ref} times a refence length l_{ref} and we can write

$$G_0 \sim \sigma_{ref}\varepsilon_{ref}l_{ref}. \tag{4}$$

3. Linear Elastic Fracture Mechanics (LEFM) considerations

In the case of uniaxial loading, we can assume that: in a stress-controlled experiment, σ_{ref} is equal to the applied

stress σ_0 and ε_{ref} to the average strain ε_0 in the Representative Volume Element (RVE); in a strain-controlled experiment, ε_{ref} is equal to the applied strain ε_0 and σ_{ref} to the average stress σ_{av} in the Representative Volume Element (RVE).
Under the assumption of linear elastic material constituents, we have, respectively for a stress- and strain-controlled experiment:

$$\varepsilon_{av} = E_{homo}\sigma_0 \qquad \sigma_{av} = E_{homo}\varepsilon_0, \tag{5}$$

where E_{homo} is a homogenized RVE Young's modulus which measures the RVE elastic response in the presence of different material phases and damage. It is worth to point out here that, as we are interested in studying debond growth in the context of transverse crack onset, RVEs are loaded in the direction transverse to the fibers in the layer where debonds are present. Furthermore, we consider RVEs that are 2-dimensional and under the assumption of plane strain or plane stress conditions. This implies, considering the elastic response of a transversely isotropic material in its plane of transverse isotropy (indeces $2-3$, index 1 corresponds to the axis of rotational symmetry) with no damage, that [20, 21]

$$E_{homo} = \frac{E_2}{1-\nu_{12}\nu_{21}} \qquad E_{homo} = E_2, \tag{6}$$

respectively for plane strain and plane stress, with E_2 the homogenized transverse Young's modulus of the ply and ν_{12}, ν_{21} the major and minor Poisson's ratios. Notice that homogenized elastic properties depend on consittuents' elastic properties and theIn the presence of damage, we can assume the homogenized Young's modulus of the damaged RVE to be a fraction of the undamaged modulus E^0_{homo} (expressed in Eq. 6):

$$E_{homo} = f\left(\Delta\theta\right) E^0_{homo}, \tag{7}$$

where $0 < f\left(\Delta\theta\right) < 1$ is a function of the damage state in the material, in this case represented by the debond half-size $\Delta\theta$ (debond size is $2\Delta\theta$). By substituting Eq. 5, Eq. 6 and Eq. 7 in Eq. 4, we have

$$G_0 \sim f\left(\Delta\theta\right) \frac{E_2}{1-\nu_{12}\nu_{21}} \varepsilon_0^2 l_{ref} \quad G_0 \sim f\left(\Delta\theta\right) E_2 \varepsilon_0^2 l_{ref}, \tag{8}$$

respectively for plane strain and plane stress conditions under applied strain ε_0, and

$$G_0 \sim f\left(\Delta\theta\right) \frac{1-\nu_{12}\nu_{21}}{E_2} \sigma_0^2 l_{ref} \quad G_0 \sim f\left(\Delta\theta\right) \frac{\sigma_0^2}{E_2} l_{ref}, \tag{9}$$

respectively for plane strain and plane stress conditions under applied strain σ_0. Notice that, incidentally: the plane strain expression in Eq. 12 is the same as the ERR expression used for *in-situ* strenght modeling in [22] and derived in [23] by considering the fiber-reinforced polymer as a 3-phase composite with one phase constituted by sharp voids (cracks); the plane stress expression in Eq. 12 is the same as the Mode I ERR in [24], derived from the definition of ERR and problem geometry.
In accord with the classic Linear Elastic Fracture Mechanics (LEFM), the Energy Release Rate is directly proportional to the crack size a [25]. Given that $a = R_f 2\Delta\theta$ for debonds, where R_f is the fiber radius, it is reasonable to assume R_f as the reference length:

$$l_{ref} = R_f. \tag{10}$$

The reference Energy Release Rate thus becomes

$$G_0 \sim f\left(\Delta\theta\right) \frac{E_2}{1-\nu_{12}\nu_{21}} \varepsilon_0^2 R_f \quad G_0 \sim f\left(\Delta\theta\right) E_2 \varepsilon_0^2 R_f, \tag{11}$$

respectively for plane strain and plane stress conditions under applied strain ε_0, and

$$G_0 \sim f\left(\Delta\theta\right) \frac{1-\nu_{12}\nu_{21}}{E_2} \sigma_0^2 R_f \quad G_0 \sim f\left(\Delta\theta\right) \frac{\sigma_0^2}{E_2} R_f, \tag{12}$$

respectively for plane strain and plane stress conditions under applied strain σ_0.

4. Similarity and geometry correction factor

In agreement with the classic Fracture Mechanics (FM) treatment [25], we can recognize in the function $f\left(\Delta\theta\right)$ of Eq. 11 and Eq. 12 the geometry correction factor ($f\left(a\right)$ or Y) that establishes the relation of similarity [26]

$$K = f\left(a\right)\sigma\sqrt{\pi a} \quad \text{or} \quad G = f^2\left(a\right)\frac{\sigma^2}{E}\pi a \tag{13}$$

between the Stress Intensity Factor (SIF) K and Energy Release Rate (ERR) G of a generic configuration of structural and crack geometry and the solution for a Center Crack (CC) in an infinite plate

$$K_{CC} = \sigma\sqrt{\pi a} \quad \text{or} \quad G_{CC} = \frac{\sigma^2}{E}\pi a. \tag{14}$$

It thus seems reasonable to look for a functional form of $f\left(\Delta\theta\right)$ in Eq. 11 and Eq. 12 among known analytical solutions of SIFs and ERRs and such that a physically-meaningful similarity between the two configurations could be established.

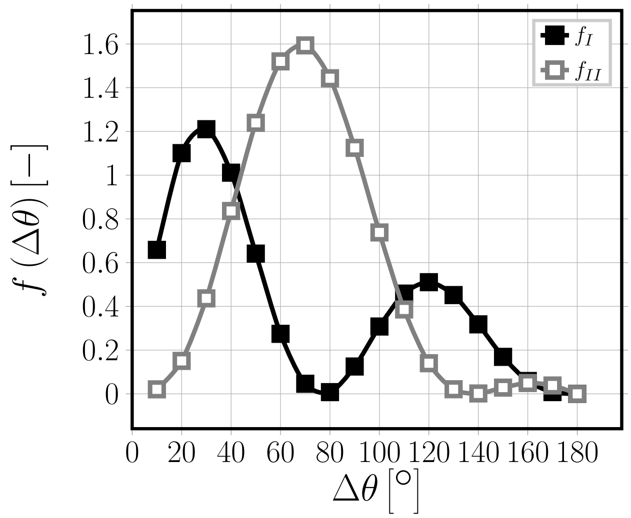

Figure 1:

- **Straight central crack in an infinite isotropic plate under far-field transverse tension [25].**

$$f_I(\Delta\theta) = \sin(\Delta\theta) \qquad f_{II}(\Delta\theta) = 0 \tag{15}$$

- **Inclined central crack in an infinite isotropic plate under far-field tension [25].**

$$\begin{aligned} f_I(\Delta\theta) &= \sin(\Delta\theta)\sin^4\left(\frac{\pi}{2} - \Delta\theta\right) \\ f_{II}(\Delta\theta) &= \sin(\Delta\theta)\sin^2\left(\frac{\pi}{2} - \Delta\theta\right)\cos^2\left(\frac{\pi}{2} - \Delta\theta\right) \end{aligned} \tag{16}$$

- **Circular crack in an infinite isotropic plate under far-field tension transverse to crack's chord [27].**

$$\begin{aligned} f_I(\Delta\theta) &= \frac{1}{2}\sin(\Delta\theta)\times \\ &\times\left(\frac{1-\sin^2\left(\frac{\Delta\theta}{2}\right)\cos^2\left(\frac{\Delta\theta}{2}\right)}{1+\sin^2\left(\frac{\Delta\theta}{2}\right)}\cos\left(\frac{\Delta\theta}{2}\right) + \cos\left(\frac{3}{2}\Delta\theta\right)\right)^2 \\ f_{II}(\Delta\theta) &= \frac{1}{2}\sin(\Delta\theta)\times \\ &\times\left(\frac{1-\sin^2\left(\frac{\Delta\theta}{2}\right)\cos^2\left(\frac{\Delta\theta}{2}\right)}{1+\sin^2\left(\frac{\Delta\theta}{2}\right)}\sin\left(\frac{\Delta\theta}{2}\right) + \sin\left(\frac{3}{2}\Delta\theta\right)\right)^2 \end{aligned} \tag{17}$$

5. Representative Volume Elements (RVEs)

6. Conclusions

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