

Research

Models analyzing the response of the adhesive joint between FRP and concrete: a comparative analysis

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Received: 20 March 2024 / Accepted: 16 August 2024

Published online: 22 August 2024

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Abstract

Fiber-reinforced polymers (FRP) for structural elements' strengthening and rehabilitation are increasingly gaining popularity because of their proven efficiency and ease of application. The response of the adhesive joint formed between the FRP and concrete is a crucial factor influencing the overall behavior of FRP structures. The analysis of the interface crack propagation is a complex task involving the consideration of multiple interacting mechanisms. This work aims to provide a background to a recently proposed model that analyzes the behavior of the adhesive joint formed at the interface between the FRP and concrete, by creating its counterpart. The study presented herein discusses a model based on fracture mechanics to obtain a reference point needed to assess the output provided by an algorithm utilizing a damage-based model for concrete and an empirical-based procedure for the loss of bond action. Typically, model validation is performed via a comparison against experimental data. However, additional insights into the interacting phenomena can be provided by comparing the results of various applicable models.

Keywords Debonding · Concrete · FRP · Fracture mechanics · Finite element modeling

1 Introduction

The increasing interest in adhesive joints for numerous applications in various industrial fields has motivated the development of experimental techniques for characterization and models for their mechanical behavior analysis and assessment. Some experimental test setups, such as single-lap-type [1], (or lap-shear [2]) are to some extent, universal while others are more industry-specific. For example, the arcane setup [3] and the semi-circular bend test [4] appear more appropriate for the automotive and aeronautical industries. Their counterpart in construction is the peeling-off test which activates simultaneously mode-I and mode-II failure. The study reported herein focuses on the application of adhesive bonding in the construction field and, more precisely on the analysis of the failure behavior of the adhesive joint between fiber-reinforced plastic (FRP) and concrete. A number of experimental, analytical, and numerical studies have investigated the interface between FRP and concrete for characterization purposes in small-scale tests and considering large structures [5–8]. In the analysis of the debonding failure for FRP structural applications, the consideration of mixed-mode conditions, or situations supposing interaction between shear (mode-II) and

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opening (mode-I) cracks [9], appears mandatory. For illustration, consider an FRP flexural reinforcement bonded to the bottom, initially horizontal surface of a beam or a slab, supposed to be subjected mainly to flexure during its design life. The applied vertical load will eventually provoke curvature in the FRP-strengthened structural element. As a result, the interface between FRP and concrete from initially horizontal will become curved and any tangent line to the deformed interface will have normal and horizontal components. In the most general case, analysis of the debonding of a thin strip of composite material adhesively bonded to a concrete member implies the consideration of multiple failure mechanisms. Local failure can occur in the concrete substrate, the adhesive layer, or the composite material. Comparing the material properties of the constituencies, concrete, adhesive, and fiber-reinforced polymer (FRP), for normal-strength concrete, the most likely scenario predicts a debonding initiation in the concrete near the adhesive join.

Available widely accepted approaches that can be implemented include explicit and implicit crack modeling. The former is associated with fracture mechanics, while the latter employs smeared crack modeling based on continuum damage mechanics. Within explicit crack modeling, the cohesive zone model has proven appropriate for simulating progressive failure if the crack trajectory is known [10–12]. Fortunately, this condition is met in the simulation of a crack propagating at the interface between FRP and concrete. On the other hand, if the crack tip trajectory isn't known in advance, fracture mechanics-based finite element simulations might involve continuous re-meshing. In a broader context, without trying to remain in the field dealing with the FRP-concrete interface investigation, alternative approaches or modifications of the standard finite element method should be used to reduce the computational burden. For example, the diffuse cohesive interface approach, presuming interface elements everywhere in the finite element mesh can be applied [13, 14]. Another option is the extended finite element modeling approach (XFEM), in which discontinuity functions are employed to simulate near-crack-tip-singularities in a standard FEM environment [15, 16]. Specifically, [15] employed the XFEM approach to model the detachment process in FRP-strengthened beams made of steel fiber-reinforced concrete. The moving mesh technique with the arbitrary Lagrangian–Eulerian formulation reportedly decreased the number of re-meshings compared to a standard finite element modeling approach [17, 18]. In contrast to the fracture mechanics-based approaches, models based on the continuum damage mechanics reproducing mechanical degradation in concrete [19–22] have drawn relatively less research attention.

The cohesive zone model employed in this study uses a damage variable in the constitutive relation for the degradable interface. The model assumes that the damage variable depends on the effective displacement jump, the material parameters defined for the interface, and the history of deformation. A recently proposed algorithm [23] also employing a damage variable, introduces an additional parameter to add more flexibility to the model by accounting for the adhesive material response—predominantly elastic or plastic. In [24], the fracture surface of dissimilar joints is analyzed by introducing parameters quantifying the fractional total fracture area and the surface sensitivity of each adherend. These parameters are employed in the load capacity evaluation of the adhesive joints.

The work presented here aims to provide a basis for comparison for a new, recently proposed approach developed to analyze the interface between FRP and concrete. Typically, the output provided by a given model is assessed via a comparison with experimental data. However, comparing the outputs of different models might also be of interest. Generally, different models reflect different points of view on the considered physical phenomena. Such points of view can be complementary to each other. Thus, continuum damage mechanics focuses on the material response before local failure, whereas fracture mechanics evaluates the likelihood of propagation of an already existing crack. Furthermore, the comparative study can be helpful in the identification of the model constants and for debugging purposes. The innovative approach, for which this study provides a background, involves a constitutive relation for concrete rooted in continuum damage mechanics, a linear elastic behavior for the transversely isotropic composite, and an original empirical procedure for the adhesive layer [25]. It will be referred to hereafter as a 'damage-based approach'. In addition to the interface failure, the damage-based approach accounts for the mechanical degradation in concrete. Unlike most reported research, it employs a smeared crack approach to model the damage accumulated in concrete. In the algorithm that uses a cohesive zone model adherents (FRP and concrete) are modeled as linear elastic materials. Some theoretical considerations are introduced and employed to obtain a tentative estimate of the model constants required in the cohesive zone model. Further tuning of the model constants is performed to reproduce best the target response curves.

The performed comparative study includes modeling and response analysis of an experimental setup that activates mixed-mode delamination in an FRP-concrete adhesive joint. The outputs provided by both approaches, the one using the damage-based model, and the other implementing the cohesive zone model, are compared in terms of numerically obtained load–displacement curves. The comparison is interpreted in light of experimental results found in the literature.

2 A benchmark experimental setup

A commonly used experimental setup [7, 26] that simultaneously activates mode-I and mode-II failure is considered (Fig. 1). A strip of composite material is adhesively bonded to a concrete block over the length L_b , as part of it remains unbounded. The unbounded part of the composite strip is then bent to form an angle between the strip and the surface of the concrete substrate. This angle, labeled θ , is traditionally referred to as a “peeling-off” angle. By varying the angle θ , the relative contribution of mode-I (opening) and mode-II (shear), or the degree of mode-mixity varies. In practice, the increasing curvature of initially straight structural elements (e.g., beams or slabs) with flexural reinforcement in composite material adhered to the soffit surface might induce such a mixed-mode failure. In this context, the design of this experimental setup designed to investigate the mixed-mode debonding is motivated by the presence of lines, tangent to the curved surface of the concrete structural element, that are not horizontal.

In Fig. 1, L_b is the initial bonded length of the composite material with a cross-sectional area A_{FRP} , ($A_{FRP} = b_{FRP}t_{FRP}$); t_{FRP} is the nominal thickness of the composite strip, and b_{FRP} is its width. P denotes the load applied at the end of the composite strip. The specific geometrical characteristics and the employed boundary conditions are described in the Sect. 5.

3 Some theoretical considerations

In fracture mechanics-based approaches, in set-ups implying a mixed-mode failure, the total energy release rate, G , is the sum of the energies for mode I, G_I , and mode II, G_{II} ,

$$G = G_I + G_{II} \quad (1)$$

The interaction between the two failure modes is addressed by defining a failure criterion [27],

$$\frac{G_I}{G_{I,F}} + \frac{G_{II}}{G_{II,F}} = 1. \quad (2)$$

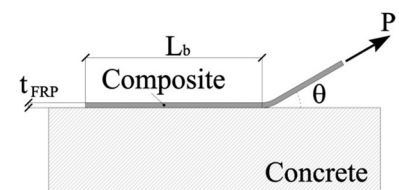
In Eq. (2), $G_{I,F}$ and $G_{II,F}$ are the fracture energies (or fracture toughness) for mode-I and mode-II conditions, respectively. To estimate the contribution of each fracture mode, a ‘phase angle’, ψ , is suggested [28],

$$\psi = \tan^{-1} \sqrt{\frac{G_{II}}{G_I}}. \quad (3)$$

By assumption, failure occurs when the energy release rate, G , reaches the fracture toughness, G_F , or taking into account Eq. (1),

$$G \equiv G_I + G_{II} = G_F. \quad (4)$$

Fig. 1 Experimental setup designed to investigate the peeling-off debonding failure



Generally, the fracture energy can be estimated by considering the energy balance during the debonding process. The work done by the system concrete-adhesive layer-composite material is equilibrated by deformation, debonding, and fracture.

Thouless and Yang [29] proposed a mixed-mode failure criterion; postulating the fracture energy as a function of the phase angle,

$$G_F(\psi) = \frac{G_F}{G_{I,F}}, \quad (5)$$

from Eqs. (1)–(3) it follows that

$$G_F(\psi) = \frac{\lambda(1 + \tan^2 \psi)}{\lambda + \tan^2 \psi}, \quad (6)$$

where

$$\lambda = \frac{G_{II,F}}{G_{I,F}}. \quad (7)$$

On the other hand, the mode-I and mode-II fracture energies can be described based on geometrical considerations, introducing a geometrical background of the mixed-mode debonding and experimental data [7, 30, 31],

$$\tan^2 \psi = \left(\frac{\Delta x}{\Delta y} \right)^2, \quad (8)$$

$$\Delta x = l(1 - r \cos(\theta)), \quad (9)$$

$$\Delta y = lr \sin(\theta), \quad (10)$$

where l denotes some deboned length and

$$r = 1 + \frac{P}{E_{FRP} A_{FRP}}. \quad (11)$$

In Eq. (11), E_{FRP} is the elasticity modulus for the composite material in a direction parallel to the fiber orientation (in an unidirectional fiber-reinforced polymer). By substituting Eqs. (9) and (10) into Eq. (8) and taking into account Eqs. (1), (3), one obtains

$$G_I = \frac{G}{(1 + Z^2)}, \quad (12)$$

$$G_{II} = G \frac{Z^2}{(1 + Z^2)}. \quad (13)$$

In Eqs. (12) and (13),

$$Z = \frac{1 - r \cos(\theta)}{r \sin(\theta)}. \quad (14)$$

4 Material models

Concrete is considered homogeneous and isotropic with a linear elastic response. Material models for the composite and the degradable interface are considered in more detail in the following.

Fig. 2 The material coordinate system for the FRP

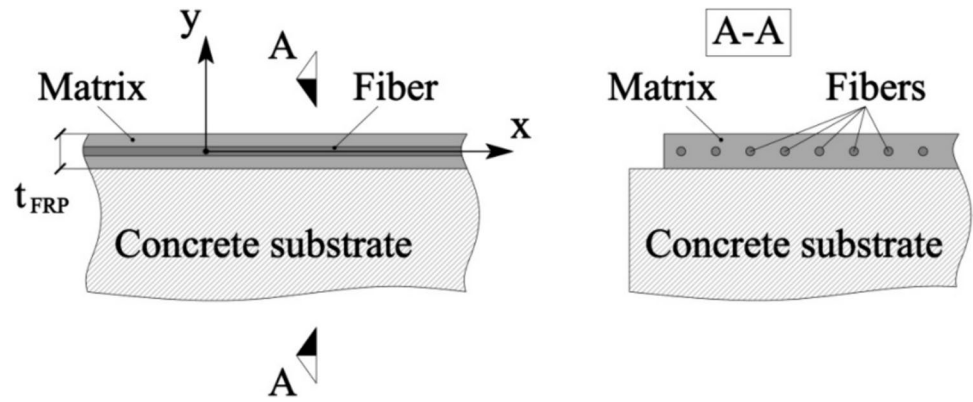
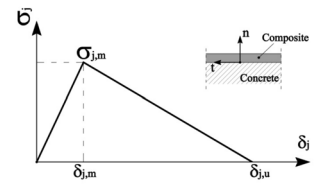


Fig. 3 Bilinear relationships for the cohesive zone model



The FRP response is generally linear and elastic until failure. Unlike concrete, in this study, the macroscopically homogeneous unidirectional composite is modeled as a transversely isotropic material. In a 2-D setting and for plane stress conditions, i.e., $\sigma_z = 0$, $\sigma_{xz} = 0$, $\sigma_{yz} = 0$, the constitutive relationship for the FRP reads

$$\begin{pmatrix} \varepsilon_x \\ \varepsilon_y \\ \gamma_{xy} \end{pmatrix} = \begin{pmatrix} \frac{1}{E_x} & -\frac{\nu_{xy}}{E_y} & 0 \\ -\frac{\nu_{xy}}{E_y} & \frac{1}{E_y} & 0 \\ 0 & 0 & \frac{1}{G_{xy}} \end{pmatrix} \begin{pmatrix} \sigma_x \\ \sigma_y \\ \sigma_{xy} \end{pmatrix}. \quad (15)$$

The elasticity constants are to be identified based on characterization tests on specimens made of the composite material. Alternatively, the effective material properties of the composite can be obtained by applying some appropriate self-consistent method, such as the mixture rule [32], if experimental data on the fibers and the epoxy matrix are available. In the numerical analysis performed herein, the composite (carbon fiber-reinforced polymer) is defined by the following material constants: $E_{frp,x} = 227 \text{ GPa}$, $E_{frp,y} = 28 \text{ GPa}$, and $\nu_{frp,xy} = 0.18$. The orientation of the material axes is shown in Fig. 2.

Bilinear relationships are assumed for mode-I and mode-II failures (Fig. 3), for the cohesive zone model employed to simulate debonding.

In Fig. 3, the subscript $j = n, t$ denotes quantities relative to orientations normal and tangential to the interface; $\sigma_{j,m}$ is the maximum tensile or shear stress, $\delta_{j,m}$ is the displacement jump corresponding to maximum stress, and $\delta_{j,u}$ is the ultimate displacement jump in the corresponding direction.

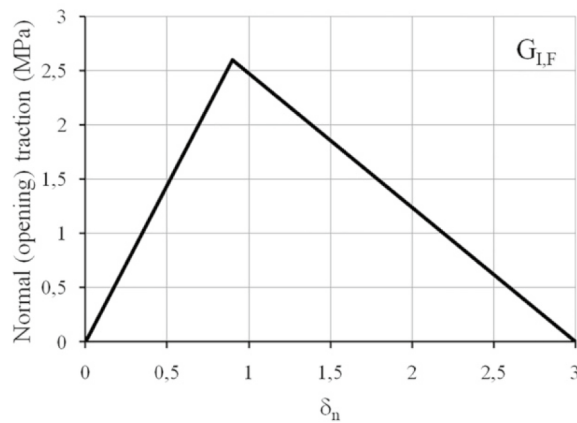
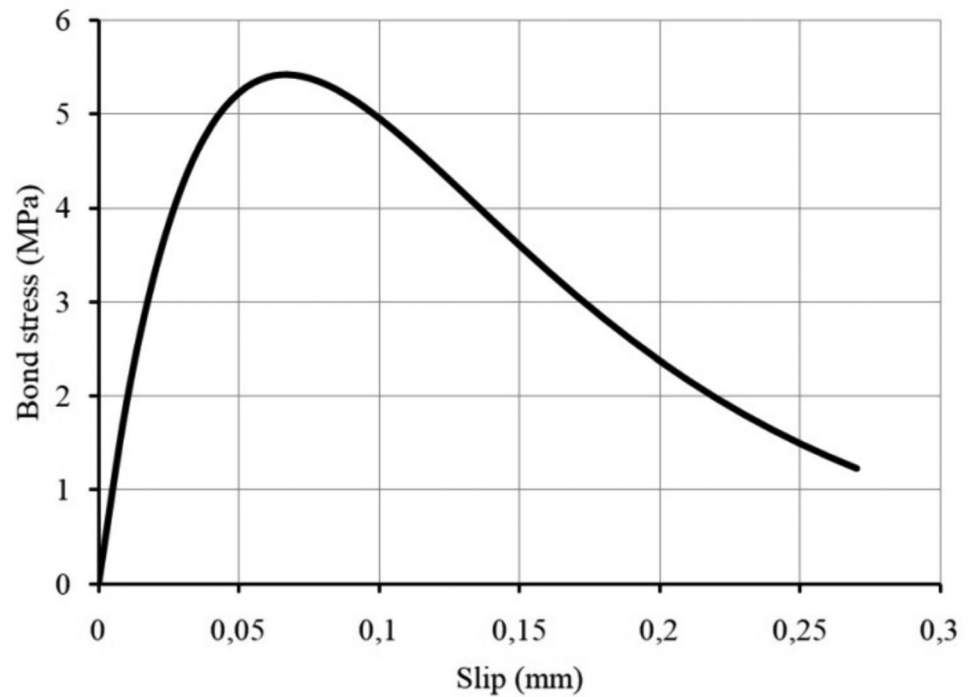
Using the postulated bilinear relationships for mode-I and mode-II, the cohesive zone model outputs an effective displacement jump s ,

$$s = \sqrt{\left(\frac{\delta_n}{\delta_{n,u}}\right)^2 + \beta^2 \left(\frac{\delta_t}{\delta_{t,u}}\right)^2}. \quad (16)$$

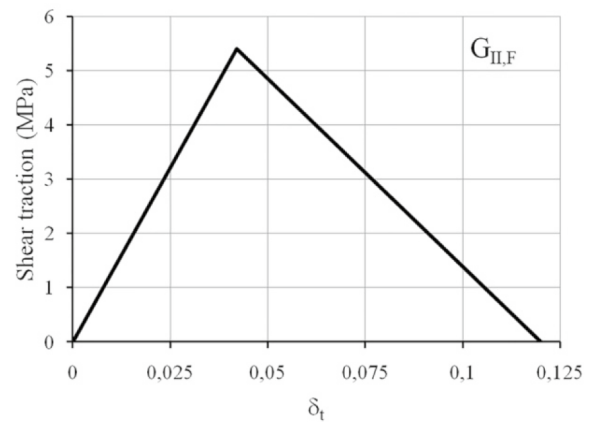
In Eq. (16), β is a weighing parameter accounting for the relative contributions of the mode-I and mode-II failure mechanisms,

$$\frac{\delta_{n,m}}{\delta_{n,u}} = \beta \frac{\delta_{t,m}}{\delta_{t,u}}. \quad (17)$$

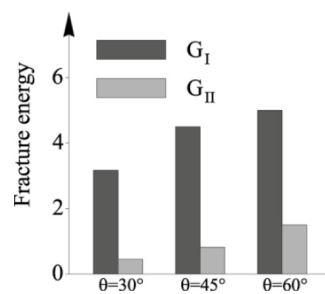
Fig. 4 A nonlinear slip-shear stress relationship [33]



a)



b)



c)

Fig. 5 The mode-I (a) and mode-II (b) fracture energies per unit width of the FRP strip evaluated for $P_u = 1635\text{N}$ for $\theta = 30^\circ$, and the relative amounts of the fracture energies for various values of the angle θ (c)

The components of the cohesive traction are then calculated by introducing a damage variable into the traction–separation relationship. The damage variable depends on the interface model parameter and the effective displacement jump. More details are provided in the [Appendix](#).

Nonlinear relationships for the interface also exist (Fig. 4) but, reportedly, the exact ‘shape’ of the constitutive relationship (e.g., bilinear, multilinear, or nonlinear) might not be a crucial factor for the output obtained by the cohesive zone model [34, 35], as long as it estimates the fracture energy as accurately as possible. For this reason, the bilinear constitutive relationship has been retained herein as the simplest one for implementation.

The fracture energy, as well as its mode-I and mode-II components, are estimated per unit width of the FRP strip based on the experimentally obtained values of the ultimate load, the assumed geometrical characteristics of the test setup, and the material properties. A trial identification is depicted in Fig. 5. As a first approximation, nominal values are assumed for maximum normal and shear tractions at the interface. The former is conventionally chosen to be identical to the tensile strength of concrete, and the latter is evaluated according to [33]. Specifically, $\sigma_{n,m} = 2.6$ MPa, and $\sigma_{t,m} = 5.4$ MPa. The maximum normal and shear displacement jumps and the displacement jumps corresponding to the maximum normal and shear tractions are then tuned to match the targeted mode-I and mode-II fracture energies. Material constants can be further tuned during the finite element simulations so that the numerical curves can match the experimentally obtained load–displacement curves if such data are available.

The relative mode-I and mode-II contributions to the overall fracture energy for various values of the peeling-off angle are illustrated in Fig. 5c, in accordance with the equations presented in the Sect. 3. As expected, and consistent with experimental data from the literature [26], the mode-I contribution is dominant for all the three considered values of the angle θ . With the peeling angle increase, the ratio $\frac{G_I}{G_{II}}$ decreases.

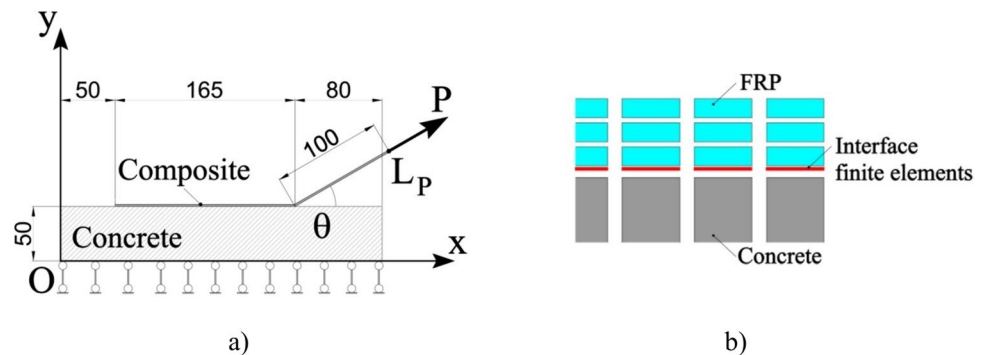
5 Finite element model

The progressive loss of bond action is simulated by finite element analysis. The general-purpose finite element code ANSYS Mechanical APDL is employed. The experimental setup comprising a thin FRP strip adhesively bonded to a concrete block is modeled within a 2-D setting. The FRP strip and the concrete block are meshed with Plane182 finite elements. Plane182 is a quadrilateral 2-D finite element with four nodes, as each node has two degrees of freedom: translations along the axes of the nodal coordinate system. The interface between FRP and concrete is modeled using the INTER202 finite element. Inter202 is a 4-node 2-D interface element. Initially, nodes are grouped into two sets, each comprising two coincident nodes with initially merged degrees of freedom. Subsequently, nodes are uncoupled, and their positions are updated as prescribed by the constitutive relation chosen for the interface.

Figure 6a presents the geometry and boundary conditions defined for the modeled experimental setup. Nodes on the bottom surface of the concrete block are restrained to move in the vertical direction,

$$u_y(x, 0) = 0, \quad \sigma_{yx}(x, 0) = 0, \quad 0 \leq x \leq 295. \quad (18)$$

Fig. 6 Geometry and boundary conditions of the modeled experimental setup (**a**); **a** zoomed-in view of the finite element mesh, including the generated interface elements shown in red (**b**). All dimensions are in millimeters



In (18), u_y denotes the y-component of the displacement vector $\mathbf{u}$. The other sides of the concrete block are not loaded

$$\begin{aligned}\sigma_x(0, y) &= 0, \quad \sigma_{xy}(0, y) = 0, \quad 0 \leq y \leq 50; \\ \sigma_x(295, y) &= 0, \quad \sigma_{xy}(295, y) = 0, \quad 0 \leq y \leq 50; \\ \sigma_y(x, 50) &= 0, \quad \sigma_{yx}(x, 50) = 0, \quad \text{for } 0 \leq x \leq 50 \quad \text{and} \quad 215 \leq x \leq 295;\end{aligned}\quad (19)$$

except for the bonded length ($y = 50, 50 \leq x \leq 215$). The load is modeled by defining concentrated forces applied to nodes at the end of the composite strip. A local coordinate system, $O'x'y'$ is defined,

$$\mathbf{e}_j = l_{ij} \mathbf{e}_i', \quad l_{ij} = \mathbf{e}_i' \cdot \mathbf{e}_j, \quad i = \{x', y'\}, \quad j = \{x, y\}. \quad (20)$$

In (20), $\mathbf{e}_i'$ and $\mathbf{e}_j$ are the unit vectors defining the axes orientation in the coordinate systems $O'x'y'$ and Oxy , respectively. Only for the loaded nodes, the nodal coordinate systems are aligned with the axes of the local coordinate system $O'x'y'$. Thus, the concentrated forces are oriented along the x' direction. Also, for the nodes where forces are applied, the motions are restrained along the y' direction to ensure that the displacement of the composite strip end will be strictly in the applied load direction (Fig. 7).

Figure 6b provides a close-up view of the generated finite element mesh. The mesh contains 15,710 finite elements: 795—for the composite, 14,750—for concrete, and 165—for the interface. The entire specimen with the generated finite element mesh is not displayed given that the element edge length is relatively small compared with the specimen's dimensions.

Finite element analysis reproduces a force-controlled experiment. The applied load P increases from zero to the prescribed ultimate value in the performed quasi-static analysis.

6 Numerical results

The main aim of this study is to provide a background for a previously published analysis of the adhesive joint failure behavior [25]. Its brief description is provided hereafter. A nonlinear relationship based on continuum damage mechanics is postulated for concrete. This enables the modeling of failure behavior in the concrete substrate. Within a 3-D setting, the composite material is considered a linear, elastic, and transversely isotropic material. A bilinear experimentally identified relationship between the shear stress and shear displacement for the interface is postulated. The algorithm presumes that the potential for shear failure is realized in the concrete substrate and the potential for shear failure- in the adhesive.

To generate an instance for comparison, a different approach is adopted. It predicates a failure zone lumped at the interface, where the finite elements with the cohesive zone model assigned are generated. By assumption, effects due to the concrete degradation are accounted for in the interface elements. The possible motivation behind this model approach is generally the conviction that degradation in concrete occurs only in a narrow zone near the interface.

The physical meaning of most of the model constants involved in the bilinear relationships retained for mode-I and mode-II failure has been discussed in Sect. 4. Additionally, $\sigma_{n,m}$, $\sigma_{t,m}$, $\delta_{n,u}$, and $\delta_{t,u}$ are illustrated in Fig. 3; the constant α defines the ratio of the displacement jump (normal or tangential) corresponding to the maximum (normal or shear)

Fig. 7 Boundary conditions at the composite strip's end

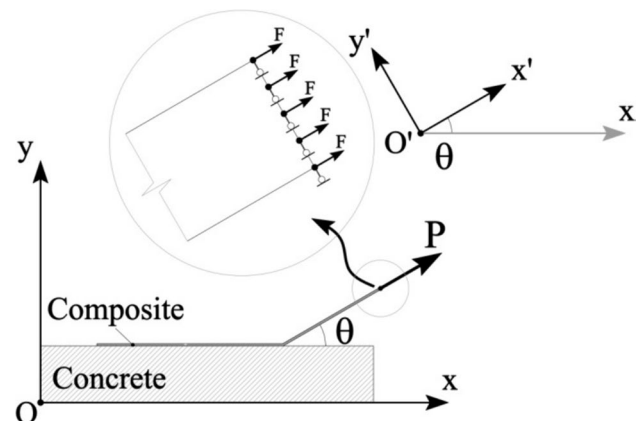


Fig. 8 Predictions of the fracture mechanics-based model for various combinations of the model constants: 'Cohesive 1'-4 (**a**); comparison between the predictions of the damage-based model [25], labeled 'Reference', and the fracture-mechanics-based model, curves 'Cohesive 5'-6 (**b**); all results are obtained for a peeling-off angle $\theta = 30^\circ$

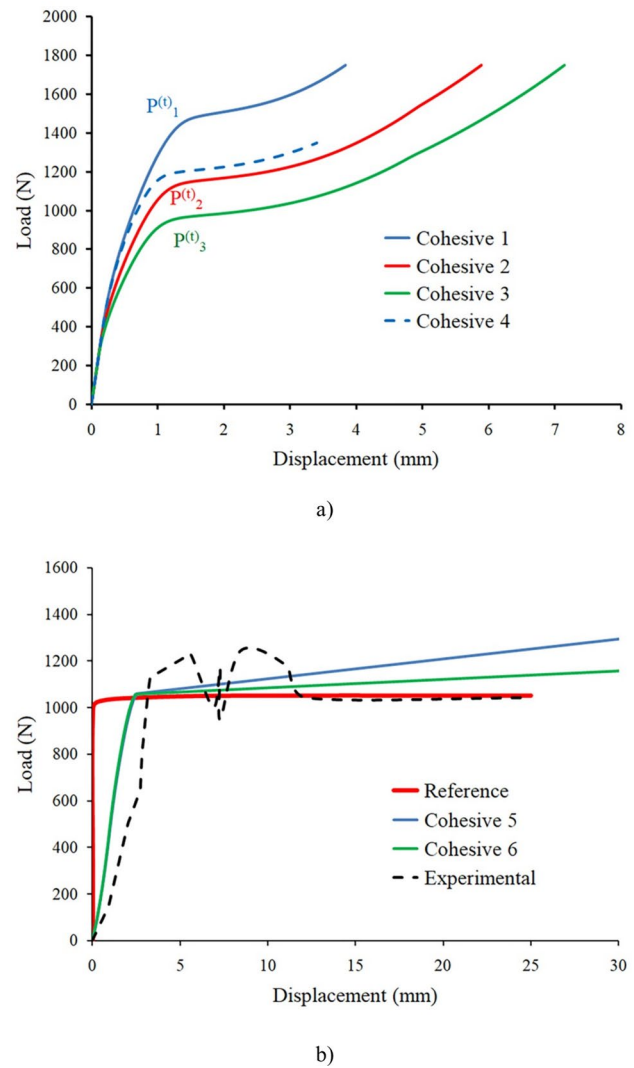


Table 1 The cohesive zone model: material model data

	$\sigma_{n,m}$ MPa	$\delta_{n,u}$ mm	$\sigma_{t,m}$ MPa	$\delta_{t,u}$ mm	α –	β –	P_u N
Cohesive 1	2.6	3	5.4	0.12	0.03	0.85	1635
Cohesive 2	2.6	3	5.4	0.12	0.03	1.5	1635
Cohesive 3	2.6	3	5.4	0.12	0.03	2	1635
Cohesive 4	2.6	2.35	5.4	0.085	0.04	0.85	1267
Cohesive-5	0.7	6	1.8	8	0.3	0.85	1267
Cohesive-6	0.7	6	1.9	5	0.3	0.85	1267

traction to the displacement jump (normal or tangential) at the local completion of debonding (Eq. 17) (i.e., $\alpha = \frac{\delta_{n,m}}{\delta_{n,u}}$); the role of the coefficient β is outlined in Eqs. 16, 17; and P_u is an experimentally identified ultimate load [7]. It should be noted that the model constants involved in the bilinear relationships for traction and shear are identified for different values of the ultimate load, P_u . For 'Cohesive 1'-3, $P_u = 1635$ N (the average of the three tests for $\theta = 30^\circ$ performed at an ambient temperature). The identification of $\sigma_{n,m}$, $\delta_{n,u}$, $\sigma_{t,m}$, and $\delta_{t,u}$ for 'Cohesive 4'-6 employs the lowest experimentally obtained value $P_u = 1267$ N.

Numerical results, obtained using the cohesive zone model, are depicted in Fig. 8a, b; the input data are summarized in Table 1. Typically, the failure behavior of the adhesive joint represented through load–displacement curves comprises two phases: an initial linear elastic phase followed by a relatively ‘horizontal’ branch of the curve associated with the debonding process. These two phases are present in all numerically obtained curves. Figure 8a provides insight into the cohesive zone model predictions for various sets of model constants. Comparing, for example, the curves ‘Cohesive-1’ and ‘Cohesive-2’, a smaller value of the parameter β , yields a larger value of the applied load, $P_i^{(t)}$, which marks the transition from the initial ‘elastic’ phase, to the following debonding phase. In the context of estimates provided in Fig. 5a, b, the mixed-mode response is dominated by the mode-I failure mechanism for a peeling-off angle $\theta = 30^\circ$. Furthermore, based on the above considerations, it would be straightforward to assume that a better-pronounced contribution of the mode-I failure (i.e., a smaller value of β) yields a larger initial ‘elastic’ domain in the adhesive joint response.

An experimentally obtained force–displacement relationship is suggested in Fig. 8b for a qualitative comparison only. Experimental data for the considered setup can be found in [7]. The qualitative analysis of the experimental data reveals that the experimentally obtained debonding behavior, defined in terms of the load–displacement relationship, either doesn’t exhibit hardening behavior in the second branch, or the hardening can be considered insignificant. In this context, it can be concluded that the numerically obtained curves ‘Cohesive-1’–4 (Fig. 8a) are characterized by inappropriately high slopes in the second phase of their response. Furthermore, in Fig. 8b, the output of the cohesive zone model is compared with results obtained by the damage-based model [25] (labeled ‘Reference’). The algorithm outlined in [25], predicts a considerably less-pronounced hardening in the debonding phase. Further tuning of the model parameters for the cohesive zone model leads to better results as apparent in curves ‘Cohesive-5’–6 (Fig. 8b). However, the damage-based model still fits the experimental data better.

In terms of the determined failure load, the experimental results exhibit important scattering—up to 29% with respect to the mean value [7]. Thus, a direct quantitative assessment of the numerical results based on predicting the adhesive joint load-carrying capacity is not considered appropriate. However, the results plotted in Fig. 8 suggest that the damage-based model slightly underestimates the ultimate load, and the cohesive zone model demonstrates a tendency for some overestimation of the load-carrying capacity of the adhesive joint.

Both models don’t capture the kinks in the load-deformation response in the second phase when debonding is initiated.

7 Concluding remarks

The presented study has employed a cohesive zone model to simulate the debonding failure of the adhesive joint between FRP and concrete. The goal is to provide a basis for comparing and assessing another recently proposed algorithm designed to analyze the interface between FRP and concrete. An experimental setup characterizing the response of the adhesive joint between FRP and concrete to mixed-mode loading conditions, referred to as a ‘peeling-off test’, has been modeled and analyzed within the comparative analysis. In the algorithmic study employing the bilinear cohesive zone model FRP and concrete have been modeled as materials of linear elastic behavior; the concrete has been considered isotropic and the FRP composite as transversely orthotropic. The model, for which this study provides a reference, accounts for degradation in concrete by postulating a constitutive relationship rooted in continuum damage mechanics; the FRP composite is modeled as a transversely isotropic material with linear elastic response; and a phenomenological relationship is postulated for the adhesive layer response.

A finite element model with boundary conditions corresponding to the chosen benchmark experimental setup has been developed within the reported study. The initial estimates of the material constants for the cohesive zone model have been derived from a purely analytical approach. Then, finer tuning has been performed during numerical simulations, to obtain results that match as closely as possible the experimental data.

As a result of the large scatter in the experimental data, a direct quantitative assessment of the compared models is difficult to formulate. However, based on the obtained results, it appears that the damage-based model slightly underestimates the load-carrying capacity of the adhesive joint whereas, the cohesive zone model slightly overestimates it. Qualitatively, both models provide output consistent with the empirical data. All obtained numerical curves contain an initial ‘elastic’ branch followed by a phase corresponding to the onset and further propagation of the debonding at the interface. Nevertheless, the response of the damage-based model is closer to the considered experimental study, by predicting the smallest hardening in the debonding phase.

Author contributions Todor Zhelyazov: - Literature review; - Conception of the work, design, and finite element analysis; - Draft and subsequent revision.

Data availability Data is provided within the manuscript.

Declarations

Competing interests The authors declare no competing interests.

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Appendix

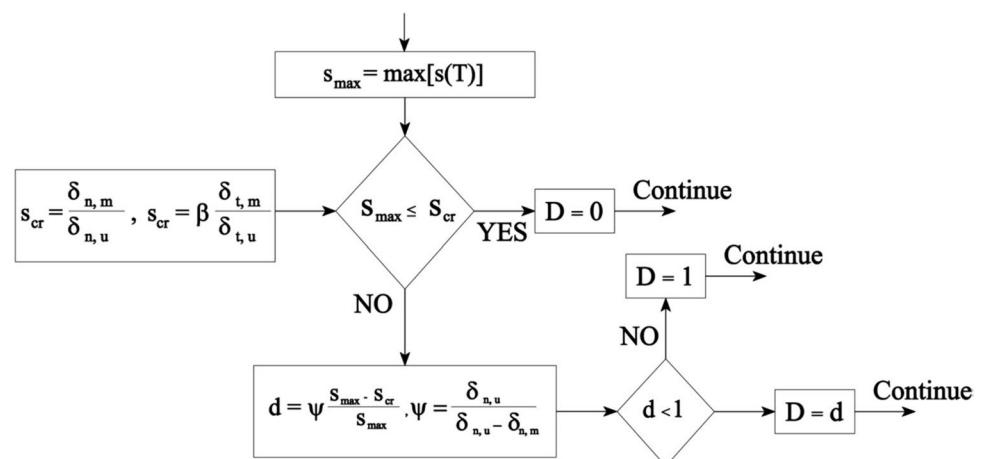
An initial estimate of the ultimate displacement jump, the displacement jump corresponding to maximum traction and the maximum traction for tension and shear is performed using the closed-form equations outlined in the Sect. 3. Then, the displacement jump-traction constitutive relations for the interface are defined according to [36],

$$\begin{aligned}\sigma_n &= E_n \delta_n (1 - D), \\ \sigma_t &= E_t \delta_t (1 - D).\end{aligned}\quad (21)$$

In Eq. (21), $E_n = \frac{\sigma_{n,m}}{\delta_{n,m}}$ and $E_t = \frac{\sigma_{t,m}}{\delta_{t,m}}$ are the initial slopes for opening and shear, respectively. The damage variable D appearing in Eq. (21) is evaluated following the chart flow below.

In Fig. 9, s denotes the effective displacement jump (Eq. 16), and s_{max} is the maximum effective displacement jump reached in the deformation time history.

Fig. 9 Calculation of the damage variable: a flowchart



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