Fatigue Behavior of FRP-to-Steel Bonded Interface: An Experimental Study with A Damage Plasticity Model

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Abstract: This study presents an experimental and theoretical investigation into the bond-slip relation of a CFRP-to-steel bonded interface under cyclic loading. It was observed that the peak interfacial shear stress during cyclic loading was lower than that during monotonic loading. A damaged-plasticity-type bond-slip relation was developed to model the constitutive behavior of the bonded interface under quasi-static cyclic loading. This was further extended by developing a model for the damage accumulation rate to account for the damage evolution under fatigue cyclic loading. The predictions from the theoretical models were found to be conservative as compared to the experimental results.

Keywords: CFRP-to-steel adhesively-bonded interface; Bond-slip behavior; Cyclic loading; Damage plasticity model; Fatigue model

1. Introduction

The strengthening of reinforced concrete structures using externally bonded (EB) fiber-reinforced polymer (FRP) laminates has become increasingly popular owing to the many advantages of FRPs, such as corrosion resistance, a high strength-to-weight ratio, and ease of installation [1-3]. Many research studies have demonstrated the effectiveness of EB shear and flexural strengthening of RC structures [4-6]. The strengthening of steel structures using carbon FRP (CFRP) laminates has also received significant attention in recent years [7]. It has been shown that CFRP laminates can enhance the the flexural capacity [8], fatigue behavior [9], and buckling [10] and torsional [11] strength of steel structures. CFRP laminates have been used for fatigue [12] and flexural [13] strengthening of steel bridges. The existing results of the long-term monitoring of CFRP-strengthened steel bridges shows large stress amplitudes on the CFRP laminates due to service loads [13, 14]. These observation indicate that fatigue is an essential issue for adhesively-bonded interface in steel bridges and has to be considered in the design.

There are many existing studies on fatigue strengthening of steel members using CFRP laminates [7, 15-27]. Existing studies have shown that CFRP laminates can be used to arrest existing fatigue cracks [28] or to prevent fatigue crack initiation [29] in steel members. Most of the studies carried out on...
fatigue strengthening of steel structures (e.g., [15-19, 21, 23-27]) have focused on demonstrating the performance of CFRP strengthening (in terms of service-life increase), rather than the behavior of the bonded interface under fatigue cyclic loading. However, the performance of fatigue strengthening solutions using CFRP laminates mainly relies on the interfacial shear stress transfer mechanism of the bonded interface to transfer loads from steel substrate to the CFRP laminate. While extensive research has been carried out on understanding the behavior of the CFRP-to-steel bonded interface under quasi-static monotonic loading [30-33], much less has been done on understanding the behavior of such bonded interfaces under cyclic loading. There are only a few studies so far that focused on understanding the behavior of CFRP-to-steel bonded interface under cyclic loading [34]. Therefore, there is a need for studies that provide a better understanding on the fatigue behavior of CFRP-to-steel bonded interface subjected to cyclic loadings.

1.1. Bond-Slip Models for FRP-to-Steel Interfaces

Bond-slip behavior indicates the relationship between the interfacial shear stress and slip, and is a key to understanding and modelling the behavior of CFRP-to-steel bonded interface under mode II loading [7, 35-38]. Much research has been conducted in determining the bond-slip behaviors of CFRP-to-steel bonded interfaces under quasi-static monotonic loading [30, 39]. The bond-slip behaviors of such bonded interfaces were found to be significantly affected by the mechanical properties of the adhesive [30]. For linear adhesives, the bond-slip behavior is typically idealized as a bi-linear curve, whereas for adhesives with a nonlinear stress-strain response, the bond-slip curves are typically idealized using trapezoidal shaped curves [30, 31]. In both the bi-linear and trapezoidal bond-slip curves, the damage is defined through a damage parameter. This parameter is zero at the onset of damage (i.e., at the end of the linear ascending branch of the bond-slip curve) and gradually varies to "1" at the onset of debonding (at the end of the linear descending branch). This damage parameter is typically calculated assuming a damaged elasticity, i.e., when unloading after the onset of damage, the interfacial shear slip is unloaded to zero at zero interfacial shear stress [36, 38]. However, a recent study by the authors [34] showed that the damaged elasticity assumption is not valid for the bond-slip behavior of a CFRP-to-steel bonded interface made using Sikadur 30 adhesive. The experimental results presented in Doroudi et al. [34] clearly showed a plastic slip during the unloading once the damage started to occur. It was also shown that the stiffness values of the unloading/reloading curves of the bond-slip relation tend to reduce with an increase in the interfacial slip. Therefore, it was clear that the behavior of the bonded interface during the softening stage was characterized by damaged plasticity. However, theoretical models capable of predicting such damaged plasticity behavior are yet to be developed for CFRP-to-steel bonded interfaces. When extending bond-slip behavior from quasi-static cyclic (i.e. low frequency cyclic, in the existing studies loading rate of 0.5mm/min or lower) to fatigue cyclic (high frequency cyclic, i.e. loading frequency of 1HZ or higher), the effects of loading cycles and loading amplitudes should also be considered. The behaviors of the bonded interfaces under mixed-mode stresses are modeled using mode I and mode II traction separation (bond-slip in mode II) models, in combination with an appropriate mixed-mode cohesive law [36, 38]. Even though the bond-slip models play an essential role in modeling the behavior of the bonded interfaces, to the best of the authors' knowledge, there are no existing theoretical models for modeling the bond-slip behavior of CFRP-to-steel bonded interface under cyclic loading.

Existing studies on CFRP-to-steel bonded interface under fatigue cyclic loading have concentrated on demonstrating the effects of fatigue loading on bond strength (i.e., the ultimate load carried by a bonded joint) [27, 40, 41], or on the performance of CFRP-strengthened full-scale girders [24, 42, 43]. Experimentally derived fatigue performance models can only be used for the respective investigated CFRP-to-steel bonded interface, and cannot be used in general. This is owing to several reasons:

(a) The interfacial fracture energy of the bonded interface depends on the bonded interface thickness, as well as the adhesive mechanical properties [30]. Therefore, an empirical model
derived based on a certain thickness and a particular adhesive, without properly accounting for the variations of interfacial fracture energy with the thickness and mechanical properties of the adhesive, cannot be applied for different bonded interface thickness and adhesive combinations.

(b) The interfacial stresses within the bonded interface depend on the geometries and mechanical properties of the adherends and adhesive [31, 36]. As the fatigue performance of the bonded interface is dependent on the loading amplitude of the bonded interface (i.e., the amplitudes of the interfacial stresses), the geometries and mechanical properties of the adhesives and adherends may affect the fatigue performance. Such parameters are not considered in the existing fatigue performance models based on experimental results; as such, these models cannot be generally applied.

Even though studies on the bond behavior of CFRP-to-steel bonded interfaces under cyclic loading are limited, there are existing studies on the behavior of metallic bonded interfaces subjected to cyclic loading [44-49]. Given that the failure mode of such bonded interfaces is also predominantly cohesion failure within the adhesive (similar to the failure of a CFRP-to-steel bonded interface) the adhesive mechanical properties are expected to significantly influence the behavior of the metallic bonded interface under cyclic loading. In a metallic-to-metallic bonded interface, the rate of crack growth can be defined as a function of the strain energy release rate [44, 49]. The well-known Paris' Law [50] is typically adapted to predict the fatigue crack growth rate, and thus the fatigue life [45-48]. Such an approach does not consider the differences in the interfacial stress states owing to the different mechanical properties of the constituents or the geometry. Therefore, the applicability of Paris' Law based approaches is limited to the exact configurations used in generating the data to calibrate those models. Moreover, when a single lap shear configuration is used, a bonded interface is subjected to mixed-mode loading, and the mode mixity ratio (i.e. the ratio between mode I and mode II loading) varies along the bond length [51]. As the respective fracture energies of the bonded interface in modes I and II are significantly different, the rate of the fatigue crack propagation can expect to be dependent on the mode mixity ratio. Nevertheless, no such consideration is given in the existing approaches for predicting fatigue performance. Even in metallic-to-metallic bonded interface tests, to the best of the authors' knowledge, there are no studies investigating the bond-slip behavior under cyclic loading.

1.2. Damage Plasticity Model for FRP-to-Concrete Interfaces

While there are no existing theoretical studies on the bond behavior of CFRP-to-steel bonded interface under cyclic loading, several analytical bond-slip models have been proposed for CFRP-to-concrete bonded interfaces under cyclic loading [52-56]. The failure mode in CFRP-to-concrete bonded interfaces (cohesion failure within concrete) is different from the failure mode in CFRP-to-steel bonded interfaces (cohesion failure within adhesive). Therefore, the results from CFRP-to-concrete bonded interfaces cannot be directly applied to a CFRP-to-steel bonded interface. However, the underlying concepts regarding the bond-slip behavior are the same between both types of bonded interfaces. Thus, it is important to review the existing theoretical studies on the bond-slip behaviors of CFRP-to-concrete bonded interfaces. Among the existing models for the constitutive modeling of the CFRP-to-concrete bonded interface under cyclic loading, only the models proposed by Carrara and De Lorenzis [55] and Zhou et al. [56] were developed based on a strong thermodynamically consistent approach. However, the Carrara and De Lorenzis [55] model assumed slip reversal at zero stress during unloading, which was found to be inconsistent with experimental observations [56]. Therefore, Zhou et al. [56] model was found to be the most accurate existing model for modelling the constitutive behavior of CFRP-to-concrete bonded interfaces under quasi-static cyclic loading.

The experimental results of CFRP-to-concrete bonded interface under quasi-static cyclic loading showed: a) a plastic slip when unloaded to zero stress, demonstrating the inaccuracy of the damaged elasticity assumption commonly used in modelling the bond-slip behavior of a CFRP-to-concrete
bonded interface; b) the shear stress becoming negative during unloading with the reversal of the slip; and c) the envelope curve of the quasi-static cyclic bond-slip curves behaving similar to the bond-slip curves under monotonic loading, indicating that the interfacial fracture energy remains constant irrespective of the difference in loading schemes [55]. Based on these observations, Zhou et al. [56] developed a constitutive model for a CFRP-to-concrete bonded interface under quasi-static cyclic loading. The developed model considered the damaged plasticity, and followed the experimental observations of the interfacial fracture energy remaining the same between monotonic and quasi-static cyclic loading schemes. The damage parameter was defined as a function of the ratio between the dissipated fracture energy and total fracture energy. The proposed model was found to be capable of accurately predicting the behavior of CFRP-to-concrete joints subjected to quasi-static cyclic loading.

Similar to Zhou et al. [56] observations, Doroudi et al. [34] experimental bond-slip behavior of CFRP-to-steel bonded interfaces under quasi-static cyclic loading showed damaged plasticity behavior, indicating the necessity for a damaged-plasticity-type model to define the bond-slip behavior under quasi-static cyclic loading. However, considering the differences between the fracture surfaces in CFRP-to-steel bonded interface and those of CFRP-to-concrete bonded interface, and that the interfacial fracture energy strongly depends on the fracture surface, the constitutive models developed for CFRP-to-concrete bonded interfaces cannot be directly applied to a CFRP-to-steel bonded interface.

Aiming to fill the gap in knowledge regarding the bond-slip behaviors of CFRP-to-steel bonded interfaces under cyclic loading, this study presents an experimental and theoretical investigation into the behavior of a CFRP-to-steel bonded interface under cyclic loading. The investigated parameters included the thickness of the bonded interface, and four fatigue cyclic loading amplitudes. A damage plasticity model for the constitutive behavior of the CFRP-to-steel bonded interface under cyclic loading is presented and the results from the model are compared with those from the experiments.

2. Experimental Program

2.1. Test Setup and Specimen Details

In total, six single-shear pull-off test specimens were fabricated and tested. The set-up used in the current tests was similar to that used by Doroudi et al. [34], and included a thick steel plate (thickness = 17 mm, length = 250 mm) supported on a rigid support block, and a pultruded CFRP plate bonded to the top surface of the thick steel plate and loaded at one end (Fig. 1). The load was applied to the CFRP plate through a grip fixed to the loading head of an MTS Landmark 100 kN servo-hydraulic machine (Fig. 1c). In addition, two sets of roller bearings (shown in Fig. 1c) were used to ensure that the load was applied in line with the CFRP plate axis. This was to minimize any effects of CFRP plate bending on the bonded joint.

MBRACE pultruded CFRP plates with a 50 mm width and 1.4 mm thickness were employed in all tests. The elastic modulus of the CFRP plates in the fiber direction was determined to be 170 GPa through material coupon testing. Two-part epoxy Sikadur 30 was used in all specimens to bond the CFRP plates to the steel substrate. The ultimate tensile strength of the adhesive was determined to be 25.3 MPa, whereas the tensile elastic modulus was determined to be 11.25 GPa through adhesive tensile coupon tests conducted as per ASTM-D3039/D3039M [57]. Prior to bonding, the surface of the steel plate was wiped with solvent, grit-blasted using 0.25 mm angular alumina grit, and cleaned using compressed air (to remove any dust and grit residue remaining on the surface) [58]. The surface of the CFRP plate was also wiped with solvent, then was lightly sanded using fine sandpaper, followed by cleaning using compressed air and acetone. The adhesive was then applied within 12 hours, and the specimens were left to cure at room temperature at least for 14 days before testing.
Out of the six specimens, the nominal thickness of the adhesive layer of four specimens was controlled to be 1 mm, and was controlled to be 1.5 mm for the other two specimens. One specimen of each adhesive layer thickness value was tested under monotonic loading, whereas the others were tested under fatigue cyclic loading.

The two specimens tested under monotonic loading were denoted M-1 and M-2, whereas the four specimens tested under fatigue cyclic loading were denoted F-1, F-2, F-3, and F-4, respectively. Details of the specimens are summarized in Table 1.

**Table 1. Details of the tested specimens.**

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Adhesive type</th>
<th>Adhesive thickness ( t_a )(mm)</th>
<th>CFRP plate elastic modulus ( E_p )(GPa)</th>
<th>Plate width ( b_p )(mm)</th>
<th>Plate thickness ( t_p )(mm)</th>
<th>Loading scheme</th>
</tr>
</thead>
<tbody>
<tr>
<td>M-1</td>
<td>Sika 30</td>
<td>1.04</td>
<td></td>
<td></td>
<td></td>
<td>M</td>
</tr>
<tr>
<td>M-2</td>
<td>Sika 30</td>
<td>1.5</td>
<td></td>
<td></td>
<td></td>
<td>M</td>
</tr>
<tr>
<td>F-1</td>
<td>Sika 30</td>
<td>0.96</td>
<td></td>
<td>170</td>
<td>50</td>
<td>F</td>
</tr>
<tr>
<td>F-2</td>
<td>Sika 30</td>
<td>1.05</td>
<td></td>
<td></td>
<td>1.4</td>
<td>F</td>
</tr>
</tbody>
</table>
2.2. Instrumentation and Loading Procedure

Thirteen strain gauges (BA120-6AA) with a gauge length of 6 mm were attached on the surface of the CFRP plate in the bonded region at intervals of 15 mm, except for the first strain gauge (counted from the loaded end, see Fig. 1a,b). The first strain gauge was installed at 5 mm from the loaded end, and the interval between the first and the second strain gauge was 10 mm. To measure the CFRP plate elastic modulus, two strain gauges were attached to the top and bottom surfaces of the CFRP plate at a 150 mm length into the un-bonded region from the loaded end (Fig. 1a,b). A photo of a prepared specimen is shown in Fig. 1b. Strain gauges on the steel plate shown in Fig. 1b were for the purpose of monitoring the strain on steel plate during loading. As steel strain was assumed to be negligible when calculating the interfacial shear slip [56,59], strain gauges on steel plate provided necessary data to verify this assumption.

The extracted strain data from the strain gauges on CFRP plate made it possible to calculate the interfacial shear stress and the slip at different locations along the bonding length, using the following equations [56, 59]:

\[
\tau_{i+1/2} = \frac{[\varepsilon_i - \varepsilon_{i-1}] (L_i - L_{i-1})}{E_p \, t_p} \quad \text{(1)}
\]

\[
\delta_{i+1/2} = \frac{\varepsilon_i - \varepsilon_{i-1} (L_i - L_{i-1})}{4} + \frac{(\varepsilon_{i-1} + \varepsilon_{i-2} (L_{i-1} - L_{i-2})}{2} + \frac{i}{2} \sum_{i=3}^{i} (\varepsilon_{i-2} + \varepsilon_{i-3} (L_{i-2} - L_{i-3}))}{2} \quad \text{(2)}
\]

In the above, \( \varepsilon_i \) is the reading of the \( i \)th strain gauge counted from the far end of the CFRP plate, with \( \varepsilon_0 = 0 \); \( L_i \) is the distance of the \( i \)th strain gauge from the far end of the CFRP plate, with \( L_0 = 0 \); \( E_p \) and \( t_p \) are the elastic modulus and thickness of the CFRP plate, respectively; and \( \tau_{i+1/2} \) and \( \delta_{i+1/2} \) are the shear stress and slip at the middle point between the \( i \)th strain gauge and the \( (i+1) \)th strain gauge, respectively.

Loading was applied using an MTS Landmark 100 kN servo-hydraulic machine in displacement control mode (for monotonic loading) and load control mode (for fatigue cyclic loading). The rate of loading in the monotonic loading was 0.5 mm/min, and the frequency of the fatigue cyclic loading was 5 Hz.

Specimens F-1 to F-4 were tested under four different fatigue cyclic loading schemes. Details of the loading schemes for each specimen are given in Table 2. In the fatigue cyclic loading, each specimen was first loaded quasi-statically to the maximum load of the predefined loading amplitude before being unloaded to zero, to determine the initial global stiffness of the bonded interface. The specimen was then subjected to constant amplitude fatigue cyclic loading for a predetermined number of cycles. The number of cycles in each step were predetermined and adjusted where necessary to ensure several loading-unloading cycles were obtained in the local bond-slip curves. Then, the fatigue cyclic loading was stopped, and the specimen was loaded and unloaded quasi-statically to measure the stiffness of the bonded interface. This procedure was repeated until sufficient data on the bond-stiffness degradation (due to fatigue) was obtained. Finally, the specimen was loaded monotonically until failure. For example, specimen F-1 was initially quasi-statically loaded to 10 kN before being unloaded to zero. Then, it was tested under a constant amplitude (6 kN–10 kN) of fatigue cyclic loading for 100,000 cycles (Table 2). After 100,000 loading cycles, the F-1 specimen was quasi-statically loaded to 10 kN, and was then unloaded to zero. This procedure was repeated 28 times (Table 2). After 2.8 million loading cycles, specimen F-1 was loaded monotonically to failure.

<table>
<thead>
<tr>
<th>F-3</th>
<th>1.07</th>
<th>F</th>
</tr>
</thead>
<tbody>
<tr>
<td>F-4</td>
<td>1.53</td>
<td>F</td>
</tr>
</tbody>
</table>

Note: M=Monotonic loading; F=Fatigue cyclic loading.
Table 2. The loading protocols for all the fatigue tests.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Loading range (kN)</th>
<th>Number of cycles (unloading/loading steps)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>1</td>
<td>2</td>
</tr>
<tr>
<td>F-1</td>
<td>6-10</td>
<td>28×100000</td>
</tr>
<tr>
<td>F-2</td>
<td>18-22</td>
<td>35×10000</td>
</tr>
<tr>
<td>F-3</td>
<td>23-27</td>
<td>–</td>
</tr>
<tr>
<td>F-4</td>
<td>28-32</td>
<td>57×10000</td>
</tr>
</tbody>
</table>

The loading amplitude was kept constant throughout the fatigue tests for specimens F-1, F-3, and F-4; however, the loading amplitude was systematically increased in several steps for specimen F-2. This was to ensure that: (a) the approximate maximum interfacial shear stress at the fatigue damage initiation could be obtained, and (b) the bond-slip behavior during a change in the loading amplitude could be obtained. Between two quasi-static loading intervals, the loading amplitude was kept the same. The exact loading amplitudes for the different steps were decided as the test progressed, and the bond-slip data was captured out of the extracted strain distributions. The final loading scheme for the F-2 specimen is shown in Table 2.

For specimen F-4, the loading amplitude was kept constant during the test, but the number of cycles between two monotonic loading intervals was increased in several steps (Table 2). This was conducted to observe if there was any effect from the number of cycles on the damage propagation rate.

3. Test Results

3.1. Failure Mode

The failed specimens are shown in Fig. 2. In specimen F-1 (with a low fatigue load amplitude), the bond did not experience any crack or failure (Fig. 2c). After 2.8 million cycles, the load grip dropped as a result of the loss of hydraulic oil pressure due to loading machine shutdown caused by a power outage. The CFRP plate in the specimen F-1 broke during the attempt to monotonically load the specimen to failure. The dominant failure mode in all of the other specimens was the cohesion failure within the adhesive. However, localized adhesion failures at the CFRP-adhesive bi-material interface (in specimens M-1, M-2, F-3, and F-4) and localized CFRP inter-laminar failures (in specimen F-2) were observed in the small regions near the far end of the bonded interface. Once the debonding propagated and the far-end region started to become active in load transfer, high peeling stresses were generated closer to the far end. Such high peeling stresses are believed to be the reason for localized adhesion and CFRP interlaminar failures at the far end. Similar failures have been observed by the other researchers [30, 34].
Fig. 2. Failure modes of the selected test specimens, (a) M-1, (b) M-2, (c) F-1, (d) F-2, (e) F-3, and (f) F-4. (Double column)

3.2. Load-Displacement Behavior

The load-displacement curves of the specimens are presented in Fig. 3. The displacements at the loaded end of the CFRP plate is the slip value at the loaded end. The slip values were calculated by using the axial strain readings of the CFRP plate in Eq. 2.

The load-displacement curves of the specimens M-1 and F-1 are presented in Fig. 3a. As expected, specimen M-1 showed a long plateau on the load-displacement curve after the bond strength was reached. For the F-1 specimen, which was loaded at a loading amplitude of 6–10kN (Table 2), no reduction in stiffness was observed during the fatigue cyclic loading. Even after 2.8 million loading cycles, the stiffness remained the same as that in the initial loading curve (Fig. 3a). The loading and unloading curves followed similar paths, with negligible hysteresis behavior. During the final
monotonic loading to failure, the strain data was lost, owing to an error in the data recording system; therefore, the final load-displacement curve could not be obtained.

The load-displacement curves of the specimens M-1 and F-2 are presented in Fig. 3b. As indicated in Table 2, the load amplitude of F-2 was adjusted during the testing. Between the first quasi-static loading cycle and second quasi-static loading cycle (i.e., after 1000 fatigue loading cycles at 18 kN–22 kN), some permanent deformations of the specimen could be observed (Fig. 3b). However, a subsequent fatigue cyclic loading at the same loading amplitude showed negligible damage propagation. After 35,000 cycles, data was obtained at 40,000, 50,000, 60,000, and 70,000 cycles. Negligible damage propagation was observed until 70,000 cycles. Therefore, after 70,000 cycles, the fatigue loading amplitude was increased to 23 kN–27 kN (i.e., 53%–63% of the bond strength). Between the first and second quasi-static loading cycles at the new loading amplitude, an increase in the permanent slip at the loaded end was observed. However, the subsequent fatigue loading cycles showed only a very slow damage propagation. After testing for 425,000 loading cycles at this loading amplitude, the loading amplitude was increased to 26 kN–30 kN (i.e., 58%–68% of the bond strength). At this loading amplitude, a gradual increase in damage could be observed (Fig. 3b). Data were obtained every 5000 loading cycles. After 500,000 loading cycles at the 26 kN–30 kN loading amplitude, the specimen was loaded monotonically to failure. During this final monotonic loading, the stiffness of the load-displacement curve of F-2 was found to be less than the initial stiffness of M-1 (Fig. 3b). However, F-2 reached a load closer to the bond strength of M-1, and also showed a loading plateau after reaching the bond strength. The reduction in the stiffness of the load-displacement curve clearly indicated the damage propagation within the bonded interface. However, F-2 reaching the bond strength and demonstrating the plateau region indicated that the undamaged bond length, at the start of the final monotonic loading, is still greater than the effective bond length of the specimen.

The load-displacement curves of specimens M-1 and F-3 are compared in Fig. 3c. The data were obtained at every 1000 cycles. Between the first and second quasi-static loading cycles, a clear increase in the loaded end deformations could be observed. Subsequently, a more gradual increase in the load-end displacement, as well as a gradual reduction in the stiffness of the curve, could be observed (Fig. 3c). After 57,000 cycles, the specimen was loaded monotonically until failure. The final load obtained for F-3 was only 36.5 kN, the lowest amongst all specimens with a 1 mm-thick adhesive layer. No plateau was observed in the load-displacement curve of F-2, with the specimen failing abruptly once the ultimate load was reached. The absence of the plateau clearly indicates that the undamaged region at the beginning of the final monotonic loading of the F-3 specimen was smaller than the effective bond length.

Discrete jumps in the load-displacement curve of F-3 were observed (Fig. 3c), owing to the re-starting of the testing in each day. At the end of testing on each day, the loading grips were released, and were tightened on the next day before resuming the tests. This relaxation of the CFRP plates and re-stressing during un-gripping and gripping is believed to be the case of the discrete jumps observed in the load-displacement curves. Similar observations can also be seen in specimen F-4 (Fig. 3d), and to a lesser extent in F-2 (Fig. 3b).

The load-displacement curves of the M-2 and F-4 specimens are presented in Fig. 3d. As expected, M-2 showed a monotonically increasing load followed by a plateau in the load-displacement curve (Fig. 3d). The initial stiffness of F-4 was similar to that of M-2. F-4 was tested at a loading amplitude of 31 kN–35 kN (i.e., 60%–70% of the bond strength). As in specimens F-2 and F-3, a jump in the loaded end deformations could be observed between the first and the second quasi-static loading cycles. The stiffness of the curve was found to gradually decrease with the increasing number of
cycles. Any difference in the rate of the stiffness reduction (as a result of the increased number of loading cycles between two data readings) was difficult to observe from the load-displacement curves. After a total of 560,000 loading cycles, F-4 specimen was loaded monotonically to failure. The ultimate load reached by F-4 was similar to the bond strength of M-2, and a small loading plateau was also observed in F-4. This indicates that although damage propagated along the bonded interface, the undamaged bond length of F-4 at the start of final loading was greater than the effective bond length.

From the above-presented results, the following clear observations could be made:

(a) The initial stiffness of the monotonically loaded specimens and specimens tested under fatigue cyclic loading are similar, provided that no damage has occurred at the bonded interface (as in F-1).

(b) While the maximum fatigue loading amplitude is less than 50% of the bond strength, no fatigue damage occurs at the bonded interface.

(c) While the maximum fatigue loading amplitude is greater than 50% and less than 63% of the bond strength, very slow damage initiation and propagation at the bonded interface occurs after a large number of loading cycles.

(d) Once the maximum fatigue loading amplitude is greater than 63% of the bond strength, fatigue damage occurs in the bonded interfaces, and the stiffness of the bonded interface tends to reduce with the increasing number of loading cycles.
Discrete displacement jumps

(b) Specimens M-1 and F-2

(c) Specimens M-1 and F-3

(d) Specimens M-2 and F-4
3.3. Axial Strain Distribution Along the CFRP Plate

The axial strain distributions along the bond length of the monotonically loaded specimens with similar adhesive thickness under fatigue cyclic loading are compared in Fig. 4, at four different displacements. The four displacements $\delta_A, \delta_B, \delta_C,$ and $\delta_D$ are shown in Figs. 3b-d. $\delta_A$ indicates the displacement at the maximum load level of the first quasi-static cyclic load. $\delta_B$ indicates the displacement at the maximum load level after applying several fatigue loading cycles and observing damage propagation of the bond. $\delta_C$ indicates the displacement at the maximum load level of the final quasi-static cyclic load. $\delta_D$ indicates the displacement in the last loading cycle (when the specimen was monotonically loaded to failure) when the curve got closest possible to the corresponding monotonically loaded specimen load-displacement curve. As specimen F-1 showed no damage at the bonded interface, this specimen is no longer discussed. At displacement $\delta_A$, specimens F-3 and F-4 showed strain distributions very similar to those of M-1 and M-2 respectively. This is also evident from the load-displacement curves of the respective specimens, where the loads of the monotonically loaded specimens are in good agreement with those of the fatigue cyclic loading specimens at displacement $\delta_A$. However, at $\delta_A$, the gradient of the strain distribution closer to the loaded end of F-2 was found to be smaller than that in M-1, and the strain value at the loaded end in F-2 was also found to be smaller than that in M-1 (Fig. 4a). The smaller strain value of F-2 relative to M-1 is also evident from the lower load of F-2 as compared to that of M-1 at displacement $\delta_A$ (Fig. 3b). In addition, the change in the gradient of the strain curve at the loaded end in F-2 (Fig. 4a) indicates softening within that region, even though the load of the F-2 specimen at this stage is lower than that of M-1.
Fig. 4. Comparisons between the strain distributions along the bonded length for the specimens under monotonic and fatigue cyclic loading at four different displacements. Specimens are (a) M-1 and F-2, (b) M-1 and F-3 and (c) M-2 and F-4. (1.5 column)

At displacements $\delta_A$ and $\delta_C$, observations could be made for all of the specimens, as follows:

(a) The strain at the loaded ends of the monotonically loaded specimens was always higher than the strain at the loaded ends of the specimens with fatigue cyclic loading. This was also supported by the higher loads of the monotonically loaded specimens as compared to those of the corresponding fatigue cyclic loading specimens at displacements $\delta_A$ and $\delta_C$. Thus, for the same displacement, monotonically loading carries a higher load. Put another way, for the same load, bonded interfaces subjected to fatigue cyclic loading show a higher deformation as compared to the monotonically loaded specimens.

(b) A plateau in the strain closer to the loaded end was observed in both the monotonic and fatigue cyclic loading specimens.
(c) The length of the plateau region in the fatigue cyclic loading specimens was larger than that of the corresponding monotonically loaded specimens, indicating additional damage propagation for the same displacement in the fatigue cyclic loading specimens.

(d) The strain value at the loaded end remained almost unchanged during the damage processes in the F-2, F-3, and F-4 specimens.

At displacement $\delta_p$, the strain distributions of the monotonically loaded and corresponding fatigue cyclic loading specimens became closer to each other. The strains at the far end of F-3 were found to be non-zero, indicating that in this stage, the undamaged bond length is insufficient to achieve bond strength. This observation is consistent with the observations from the load-displacement curves (Fig. 3c).

3.4. Interfacial Shear Stress Distribution

In Fig. 5, the interfacial shear stress distributions (obtained using Eq. 1 and the CFRP plate axial strains) of the specimens F-2, F-3, and F-4 at the displacements $\delta_A$, $\delta_B$, $\delta_C$, and $\delta_D$ (Figs. 3b-d) are compared with the interfacial shear stress distributions of the corresponding monotonically loaded specimens at the same displacements. Already at displacement $\delta_A$, the loaded end interfacial shear stress in F-2 was found to be closer to zero, indicating the initiation of debonding. However, none of the other specimens showed damage to the interface at displacement $\delta_A$, and instead showed increasing interfacial shear stresses towards the loaded end. It is also to be mentioned that both specimens F-3 and F-4 were in fact loaded to a higher load than F-2 at $\delta_A$. However, F-2 had been subjected to a much higher number of loading cycles (compared to F-3 and F-4), which resulted in damage propagation before the displacement $\delta_A$ was reached. Therefore, this observation clearly indicates the effects of the number of loading cycles and fatigue loading amplitude on the propagation of damage.

At displacements $\delta_B$ and $\delta_C$, results were observed for all specimens as follows:

(a) A debonding initiation and propagation closer to the loaded end was observed in all specimens (Fig. 5).

(b) The peak interfacial shear stress of the fatigue cyclic loading specimens was found to be smaller than that of the monotonically loaded specimens (Fig. 5). The average peak interfacial shear stress of the fatigue cyclic loading specimens was found to be dependent on the loading amplitude (Table 3). For the F-2 specimen loaded at 68% of the bond strength, the average peak interfacial shear stress was found to be 16.07 MPa (i.e., 68% of the peak interfacial shear strength). Peak interfacial shear strength in this paper is defined as the ultimate interfacial shear strength of the monotonically loaded specimen. For the F-3 specimen loaded at 74% of the bond strength, the average peak interfacial shear stress was found to be 17.38 MPa (i.e., 73% of the peak interfacial shear strength). Finally, for the F-4 specimen loaded at 70% of the bond strength, the average peak interfacial shear stress was found to be 16.39 MPa (i.e., 69% of the peak interfacial shear strength).

(c) The distance from the loaded end to the point where the peak interfacial shear stress occurred was found to be longer in the fatigue cyclic loading specimens than in the corresponding monotonically loaded specimens. This indicates additional damage propagation in the fatigue cyclic loading specimens.

(d) The peak interfacial shear stresses during the final monotonic loading of F-2 and F-3 were similar to that of M-1, and the peak interfacial shear stress of F-4 during the final monotonic loading was similar to that of M-2. This indicates the existence of an un-damaged bonded
region in the fatigue cyclic loading specimens at the beginning of the final monotonic loading step.

Fig. 5. Comparisons between the interfacial shear stress distributions along the bonded length for the specimens under monotonic and fatigue cyclic loading at four different displacements. Specimens are (a) M-1 and F-2, (b) M-1 and F-3 and (c) M-2 and F-4. (1.5 column)
Table 3. Bond-slip properties determined from the experiments.

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Loading type</th>
<th>Peak interfacial shear stress $\tau_{1,\text{exp}}$ (MPa)</th>
<th>Slip at the initiation of softening $\delta_i$ (mm)</th>
<th>Slip at the initiation of debonding $\delta_u$ (mm)</th>
<th>Interfacial fracture energy $G_f$ (N/mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>M-1</td>
<td>Monotonic</td>
<td>23.36</td>
<td>0.028</td>
<td>0.144</td>
<td>1.68</td>
</tr>
<tr>
<td>M-2</td>
<td>Monotonic</td>
<td>24.20</td>
<td>0.035</td>
<td>0.188</td>
<td>2.28</td>
</tr>
<tr>
<td>F-1</td>
<td>Fatigue</td>
<td>-</td>
<td>-</td>
<td>-</td>
<td>-</td>
</tr>
<tr>
<td>F-2</td>
<td>Fatigue</td>
<td>16.07</td>
<td>0.020</td>
<td>0.099</td>
<td>0.79</td>
</tr>
<tr>
<td>F-3</td>
<td>Fatigue</td>
<td>17.38</td>
<td>0.022</td>
<td>0.11</td>
<td>0.97</td>
</tr>
<tr>
<td>F-4</td>
<td>Fatigue</td>
<td>16.39</td>
<td>0.025</td>
<td>0.16</td>
<td>1.35</td>
</tr>
</tbody>
</table>

3.5. Bond-Slip Behavior

Bond-slip relationship for each specimen at different locations was obtained from the CFRP plate axial strain measurements using Eqs. 1 and 2. Selected bond-slip curves for each specimen are given in Figs. 6–9.

From the experimental bond-slip curves, the following observations were made:

(a) For monotonically loaded specimens, the bond-slip curves at different locations along the bond-length were shown to be similar (Fig. 6), demonstrating the consistency of bond-slip curves obtained using the CFRP plate axial strains.

(b) Most of the bond-slip curves of the monotonically loaded specimens started with low stiffness, but the stiffness gradually increased with the increasing load (Fig. 6). In specimens subjected to fatigue cyclic loading, at the early stages of fatigue cyclic loading, an increase in the stiffness of the loading curve was also observed. The stiffness of the curve beyond the initial low-stiffness region was found to be similar in the specimens subjected to monotonic loading and fatigue cyclic loading (Fig. 7).

(c) Once in the softening stage and unloaded to zero load, the interfacial shear stresses were shown to become negative in the fatigue cyclic loading specimens (Fig. 7). A similar observation was found for the CFRP-to-steel bonded interfaces subjected to quasi-static cyclic loading [25]. As the unloading stiffness is smaller than the initial loading stiffness, the interfacial shear stress reaches zero before the total load of the system becomes zero. Further unloading results in the reversal of the interfacial shear stress transfer, and thus the interfacial shear stresses take negative values [34],

(d) Under constant-amplitude fatigue cyclic loading, the interfacial shear stress was found to reach a maximum, and then to decrease monotonically (Fig. 7). This indicates that under a constant fatigue cyclic loading amplitude, the bond-slip behavior could be idealized as a bi-linear curve, similar to the idealizations made for bond-slip curves under monotonic loading. However, the interfacial fracture energy of the bonded interface subjected to fatigue cyclic loading remained lower than that of a bonded interface subjected to monotonic loading (Table 3).

(e) During constant-amplitude fatigue cyclic loading, the stiffness of the loading-unloading curves tends to decrease gradually (Fig. 7). This decrease in stiffness indicates damage. Therefore, a gradual decrease in stiffness indicates a gradual increase in the damage parameter due to the fatigue cyclic loading.
(f) During the fatigue cyclic loading, an increase in loading amplitude results in an increase in the interfacial shear stress (see Fig. 8). In this figure, the ascending branch of the loading curve during the increase in loading was found to follow the same slope as the final monotonic loading curve at the lower loading amplitude. It was also found that, during the load increase, the interfacial shear stress will never surpass the envelope of the bond-slip curve (i.e., the bond-slip curve of the monotonically loaded specimens). As found in the experiments, once the damage is induced by fatigue cyclic loading, the dissipated energy owing to the damage cannot be recovered. Therefore, as long as local points have endured damage owing to fatigue cyclic loading, the total fracture energy will always remain lower than the fracture energy of the same bonded interface under monotonic loading.

(g) During the final monotonic loading, the bond-slip curves tend to converge towards the envelope curves (Fig. 9). The bond-slip curves under post-fatigue loading converging to the envelope curves indicates that the residual fracture energy of the bonded interface at any damage state is the same as the residual fracture energy of the monotonically loaded bonded interface at the same damage state.

Fig. 6. Bond-slip relations for the specimens (a) M-1 and (b) M-2 under monotonic loading.
Fig. 7. Comparisons between the bond-slip relations under monotonic and fatigue cyclic loading at identical locations for the specimens (a) M-1 and F-2, (b) M-1 and F-3 and (c) M-2 and F-4. (Single column)

Fig. 8. Comparisons between the bond-slip relations for the specimen M-1 (under monotonic loading) and F-2 (under two different fatigue cyclic loading regime). (Single column)
Fig. 9. Comparisons between the bond-slip relation in the specimens F-2 and M-1 at different lengths from the loaded end. (a) 105mm; (b) 120mm

4. Theoretical Modeling of the Bond Behavior Under Cyclic Loading

4.1. General

As explained in the introduction, modelling the behavior of bonded interfaces under mode II loading is a key initial step in modelling behaviors under mixed-mode loading. As this study is aimed at the mode II behavior of the CFRP-to-steel bonded interface, only the theoretical modelling related to mode II behavior is discussed.

Among the existing theoretical approaches for modelling the behavior of bonded interfaces under mode II loading, only a few have addressed the non-linear behavior of the constituents [35, 37, 54, 59]. Amongst these models, the Yuan et al. [60], De Lorenzis and Zavarise [35], and Fernando et al. [37] models assumed the damaged elasticity behavior of the bond-slip models; therefore, these models do not agree with the damaged-plasticity type of behavior observed in the experimental results of this study. The Martinelli and Caggiano [54] model considered the damaged-plasticity type of bond-slip behavior, and was therefore deemed the most suitable for modelling the behavior of CFRP-to-steel bonded interface investigated in the current study. Therefore, the numerical procedure proposed in Martinelli and Caggiano [54] was adopted in this study, with a modification to the bond-slip behavior under cyclic loading (both quasi-static cyclic and fatigue cyclic loading).
In modelling the behavior of CFRP-to-steel bonded interface investigated in this study:

(a) it was assumed that the bonded interface is only subjected to shear deformations; this is a
common assumption made when studying the behavior of such bonded interfaces in single
shear pull-off tests, and was verified based on experimental evidence [51]; and
(b) it was further assumed that the interfacial shear stresses are uniformly distributed across the
adhesive layer thickness, and that the CFRP plate and steel plate are subjected to uniform
axial stresses [37, 51].

With these assumptions, the governing differential equation for the bonded interface in a single shear
pull-off test is as follows [51]:

\[
\frac{d^2 \delta}{dx^2} - \left( \frac{1}{E_{p}t_{p}} + \frac{b_{p}}{E_{st}b_{st}t_{st}} \right) f(\delta) = 0
\] (3)

Here, \( \delta \) is the relative slip between the two adherents. \( E_{p}, b_{p}, \) and \( t_{p} \) are the elastic modulus, width,
and thickness of the CFRP plate, respectively. Likewise, \( E_{st}, b_{st}, \) and \( t_{st} \) are the elastic modulus,
width, and thickness of the steel plate, respectively. \( f(\delta) \) represents the bond-slip relationship (i.e., the
relationship between the interfacial shear stress (\( \tau \)) and the slip (\( \delta \))).

4.2. Bond-Slip Model for Cyclic Loading

4.2.1 Quasi-static cyclic loading

A bi-linear bond-slip model under quasi-static cyclic loading was adopted in this study, as illustrated
in Fig. 10. In the bond-slip model proposed for quasi-static cyclic loading, the stiffnesses of the
unloading and reloading curves were assumed to be the same, provided no further damage had
occurred during the unloading process.

Existing experimental investigations on CFRP-to-steel bonded interfaces under quasi-static cyclic
loading [34] have shown that when unloaded during the softening region, a residual slip exists at zero
stress. Thus, a damaged-plasticity-type model is required to model the bond-slip behavior of a CFRP-
to-steel bonded interface under quasi-static cyclic loading. The experimental bond-slip curves under
fatigue cyclic loading presented in this study also show that a damaged-plasticity-type model is
necessary for modelling the bond-slip behavior under fatigue cyclic loading. Zhou et al. [56] defined a
damage parameter as a function of the ratio between the dissipated energy (\( w_{d} \)) at a particular point
and the total fracture energy (\( G_{f} \)); this parameter was found to provide good agreement with
experimental results. Therefore, a similar approach was used in this study. In Fig. 10a, the total
interfacial fracture energy is considered as the total area under the envelope of the bond-slip curve
while the dissipated energy is in the hatched area (i.e., area 0134). Assuming that the bond-slip
behavior during positive and negative loading is antisymmetric with respect to the slip axis, the
damage parameter, \( D \) can be defined as follows:

\[
D = f \left( \frac{w_{d}}{G_{f}} \right)
\] (4)

In Eq. 4, the dissipated energy can be expressed as follows:

\[
w_{d} = \int_{0}^{\delta f} \tau \, d\delta - \frac{\tau_{f}^{2}}{2(1 - D)K_{e,0}}
\] (5)
In the above, $\delta_r$ and $\tau_r$ represent the slip and interfacial shear stress, respectively, when unloading occurs (i.e., point 3 in Fig. 10a). When unloading/reloading occurs at point 7 in Fig. 10b, the dissipated energy is the sum of the hatched areas, i.e., areas 0134 and 4378. Similarly, in Fig. 10c, the dissipated energy is the sum of areas 0134 and 4578. If the relationship between the damage parameter and $w_d/G_f$ in Eq. 4 is known, the damage parameter at any step can be incrementally solved for using Eq. 5.

Two scenarios of unloading/reloading in the softening range are considered in this model, as illustrated in Fig. 10b and Fig. 10c. In the first scenario (Fig. 10b), when unloading occurs in the positive softening stage and reloading occurs before further damage (e.g., path 0134543 in Fig. 10b), the unloading and reloading stiffnesses are assumed to be the same [56]. During the reloading process, when the reloading curve meets the envelope curve, further damage will occur, and the reloading curve will follow the envelope curve until the next unloading point (e.g., path 5437 in Fig. 10b). In the second scenario (Fig. 10c), reloading/unloading (e.g., path 0134578 in Fig. 10c) occurs in the negative softening range, and it is assumed that the slip value cannot be larger than plastic slip in the previous unloading process (i.e., the slip value at point 4), when the interfacial shear stress reaches zero. After a certain amount of damage, the total plastic slip is assumed to be zero and elastic damage model is applied [56].

For any damage parameter $D$, the unloading/reloading stiffness of the bond-slip model for quasi-static cyclic loading can be calculated as follows:

$$K_d = (1 - D) K_{e,0}$$

(6)

With the definition above, the local bond-slip relation can be expressed as follows:

$$f(\delta) = (1 - D) K_{e,0}(\delta_r - \delta_p)$$

(7)

Here, $\delta_p$ is the plastic slip when unloading/reloading occurs at $\delta_r$. Then, the governing equation (i.e., Eq. 3) can be solved using a finite difference method (FDM) [54].

Fig. 10. The proposed damage plasticity model: (a) definition of the damage in the first cycle. Definition of damage in the succeeding cycles: (b) unloading/reloading happens in the positive softening range and reloading happens before further damage occurs (c) reloading/unloading happens in the negative softening range.
softening stage before further damage in unloading (c) reloading/unloading happens in the positive
and negative softening range. (Double column)

To use the above method, the function for the damage parameter given in Eq. 4 should be known. The
experimental bond-slip curves given in Doroudi et al. [34] for CFRP-to-steel bonded interface under
quasi-static cyclic loading were used to calibrate the function in Eq. 4. In using the bond-slip data to
calibrate the model in Eq. 4, the following steps were followed:

Step 1: The envelope of the bond-slip curves under quasi-static cyclic loading was obtained. Envelope curves were obtained for the ascending and descending branches of the curves by using an appropriate polynomial curve to fit the data. The negative regions of the bond-slip curves were ignored.

Step 2: Using the envelope bond-slip curves, the initial elastic modulus of each curve was obtained. The average secant stiffness was obtained from five points (corresponding to 0.2, 0.4, 0.6, 0.8, and 1.0 of the interfacial shear strength) within the ascending branch of each curve.

Step 3: The stiffness ($K_d$) of the unloading/reloading curves was determined as the average slope value (i.e., the average slope of the curves obtained by connecting the minimum and maximum points) of the two branches of the hysteresis loop indicated by the bond-slip curves. Then, the damage parameter was calculated, based on the following equation.

$$ D = 1 - \frac{K_d}{K_{e,0}} $$

(8)

Step 4: Based on the unloading/reloading stiffness values obtained from the previous step, the dissipated energy was obtained as the area enclosed by the bond-slip curve and unloading path. It should be pointed out that the lowest point of unloading was determined as the point with the lowest interfacial shear stress. The energy ratio was then determined using Eq. 4, and a function of the damage parameter defined by $w_d/G_f$ was obtained as follows.

$$ D = -\left(\frac{W_d}{G_f}\right)^2 + 2 \left(\frac{W_d}{G_f}\right) $$

(9)

The data are shown in Fig. 11. A second-order polynomial is chosen to satisfy the requirement that the damage parameter equals "1" when the residual fracture energy is zero (or when the specimen is fully cracked). This function is used to obtain the damage parameter and corresponding plastic slip at different points along the softening range, considering the bi-linear bond-slip relationship.
Fig. 11. The relationship between the damage parameter and the ratio between the dissipated energy and the total fracture energy while plastic slip is greater than zero. (1.5 column)

In Fig. 12, predictions using the above-described method for the bond-slip curves are compared with the experimentally obtained bond-slip models of Doroudi et al. [34] for CFRP-to-steel bonded interface under quasi-static cyclic loading. The proposed model was found to provide accurate predictions.
Fig. 12. Comparisons between the bond-slip relations from the test results of Doroudi et al. [25] and the proposed numerical model: (a) CFRP-to-steel bonded interface with 1-mm thick adhesive layer tested under quasi-static cyclic loading (T1C(2)): L=45mm; (b) CFRP-to-steel bonded interface with 1.5-mm thick adhesive layer tested under quasi-static cyclic loading (T1.5C): L=30mm; (c) CFRP-to-steel bonded interface with 2-mm thick adhesive layer tested under quasi-static cyclic loading (T2C): L=105mm. (Double column)

4.2.2 Fatigue cyclic loading

When a bonded interface is subjected to fatigue cyclic loading, the damage accumulation owing to fatigue cyclic loading should be considered when defining the bond-slip relationship $f(\delta)$. Based on the experimental observations presented above, a bi-linear bond-slip relationship was assumed for $f(\delta)$. With this assumption, the local bond-slip relation is defined by the expression given in Eq. 7.

Unlike a bond-slip model for quasi-static cyclic loading, a bond-slip model for fatigue cyclic loading also requires consideration of different loading and unloading paths, to account for the progression of damage owing to the fatigue cyclic loading.

As observed during the experiments, damage owing to fatigue cyclic loading could initiate prior to reaching the peak interfacial shear strength. The experimental results showed that the minimum interfacial shear stress (within the initial ascending branch of the local bond-slip curve) required to initiate damage owing to fatigue cyclic loading is 15.06 MPa (i.e., the interfacial shear stress value corresponding to a 27 kN load). Any load resulting in interfacial shear stresses less than 15.06 MPa was found to cause no damage.

Careful observation of the experimental results showed that the damage propagation under fatigue cyclic loading is dependent on the existing state of damage and loading amplitude. Therefore, calculation of the damage parameter requires an incremental process. A regression of the damage accumulation rate from the test data was conducted with regards to the damage parameter at each step, as well as the maximum and minimum slip range corresponding to the maximum and minimum fatigue cyclic loading. As the hysteresis of the unloading/reloading curve was ignored while calculating the damage parameter, a damage transition stiffness was defined, to account for the
damage accumulation between consecutive loading cycles (Fig. 13) [43]. Notably, the transition stiffness only has a numerical meaning; thus, the continuous fatigue cyclic bond behavior can be simulated.

Considering the methodology adopted in this study, i.e., with an unloading and a transition cycle, the unloading stiffness (shown by red arrows in Fig. 13) always indicates the stiffness of the unloading curve at a given number of fatigue loading cycles, whereas the transition stiffness (shown by green arrows in Fig. 13) accounts for the damage accumulation for a given number of fatigue loading cycles. For this methodology to work, the number of loading cycles must always be larger than 1. If the number of loading cycles is equal to 1, the model proposed for quasi-static cyclic loading should be used.

![Graph](image)

**Fig. 13.** The schematic of unloading/reloading damage parameter (in red) and the transition damage parameter. (Single column)

For the unloading and transition curves, the damage increment at any time step \((j+1)\) was defined in this study as follows:

\[
\begin{align*}
D_{j+1}^{d} - D_{j}^{d} & = \alpha \times (1 - D_{j}^{d}) \times e^{\beta D_{j}^{d}} \times \left( \frac{\delta_{\max}^{d}}{\delta_{1}^{d}} \times \left( \frac{\delta_{\max}^{r} - \delta_{\min}^{r}}{\delta_{1}^{r}} \right) \right) \\
\end{align*}
\]  

(10)

Here, \(D_{j+1}^{d}\) and \(D_{j}^{d}\) are the damage parameters (unloading damage parameter or transition damage parameter) in the \((j+1)\)th and \(j\)th step, respectively. \(N_{j+1}\) and \(N_{j}\) are the numbers of loading cycles in the \((j+1)\)th and \(j\)th step, respectively. \(\delta_{\max}^{d}\) and \(\delta_{\min}^{r}\) are the unloading and reloading slip corresponding to loading amplitude of the \(j\)th step, respectively. \(\delta_{1}\) is the slip at the peak interfacial shear strength, \(\alpha\) and \(\beta\) are empirical constants, and depend on the adhesive type used in the bonded interface. The values of the empirical constants \(\alpha\) and \(\beta\) were obtained for the CFRP-to-steel bonded interface with Sikadur 30 adhesive based on the experimental data above. The process of obtaining the data to calibrate the empirical constants comprised the following steps:

**Step 1:** The envelope bond-slip curves under fatigue cyclic loading in the ascending and descending branches were obtained. A straight line was used to connect the gaps (caused by the hysteresis behavior) between consecutive unloading points.
Step 2: Using the envelope bond-slip curves, the initial elastic modulus of each curve was obtained. When obtaining the average initial elastic modulus of the curves, three points within the ascending branch of the curve (corresponding to 20%, 40%, and 60% of the interfacial shear strength) were used.

Step 3: The stiffness values of the unloading curves after the fatigue cyclic loading steps were determined as the average slope value of the two branches of the hysteresis loop indicated by the bond-slip curves. Eq. 8 was used to determine the damage parameter. The corresponding number of loading cycles and \( \delta_{\text{max}} \) and \( \delta_{\text{min}} \) values were also recorded.

Step 4: All of the unloading and reloading interfacial shear stresses and slips (\( \tau^j \) and \( \delta^j \)) were obtained. The number of loading cycles (which induce the damage in the bond) was also considered, so as to investigate its relationship with the damage parameter.

Step 5: \( \frac{D^{j+1} - D^j}{N_{j+1} - N_j} \) was calculated and recorded, together with the extracted unloading and reloading slips in the bond-slip curves, corresponding number of loading cycles, and damage parameters.

Step 6: Using the extracted data, a regression analysis was conducted to determine the values of the constants \( \alpha \) and \( \beta \) for the unloading model, thereby showing the unloading path of the fatigue cyclic model.

Step 7: Using the extracted data, the constants \( \alpha \) and \( \beta \) were also obtained through regression for a transition model, thereby showing the loading path of the fatigue cyclic model.

With the above-presented procedures, the values of the constants \( \alpha \) and \( \beta \) for the unloading model were found to be \( 7.45 \times 10^{-5} \) and \( -3.17 \), respectively. The values of constants \( \alpha \) and \( \beta \) for the transition model were found to be \( 6.25 \times 10^{-4} \) and \( -4.67 \), respectively. In terms of the data used in the analysis, the data obtained from the first 50 mm of the bond length were ignored. The coefficients of determination of the regression models, \( R^2 \), were found to be 98% and 96% for the unloading model and transition model, respectively, showing excellent agreement with the data. The final models for the damage increment of the CFRP-to-steel bonded interface with Sikadur 30 adhesive under fatigue cyclic loading are as follows:

\[
\frac{D^{j+1} - D^j}{N_{j+1} - N_j} \text{Unloading/reloading} = 7.45 \times 10^{-5} \times (1 - D^j) \times e^{-3.17D^j} \times \left( \frac{\delta^j_{\text{max}}}{\delta^j_{1}} \right) \times \left( \frac{\delta^j_{\text{max}} - \delta^j_{\text{min}}}{\delta^j_{1}} \right) \tag{11}
\]

\[
\frac{D^{j+1} - D^j}{N_{j+1} - N_j} \text{Transition} = 6.25 \times 10^{-4} \times (1 - D^j) \times e^{-4.67D^j} \times \left( \frac{\delta^{j+1}_{\text{max}}}{\delta^j_{1}} \right) \times \left( \frac{\delta^{j+1}_{\text{max}} - \delta^j_{\text{min}}}{\delta^j_{1}} \right) \tag{12}
\]

When obtaining the constants for the models presented in Eqs. 11 and 12 via the experimental data, scatter could be observed in the data. This is shown in Fig. 14, through the variation of the damage parameter with the number of loading cycles at different locations of the bonded interface for the F-3 specimen (specimen with the least scatter). Considering that F-3 was tested under constant amplitude loading, and the equal number of fatigue loading cycles between two consecutive quasi-static loading cycles, the variation of the damage parameter with the number of loading cycles could be expected to
be similar along the bond length. However, the gradient of the curves in Fig. 14 tends to vary along the bond length. Even though a uniform stress distribution along the adhesive thickness is assumed in determining the bond-slip behaviors, in reality, the stresses vary across the height of the adhesive layer of the CFRP-to-steel bonded interface [51]. The bonded interfaces of the CFRP-to-steel bonded interface are subjected to mixed-model loading, even though the mode II loading is the largest contributor to failure propagation under monotonic loading [51]. Closer to the loaded end, the peak interfacial shear stresses observed in the experimentally obtained bond-slip curves often showed significantly higher values than those obtained from the rest of the bond length [30]. This is due to the existence of compressive mode I stresses closer to the loaded end [51]. Such complex stress states may affect the fatigue crack propagation within the bonded interface, and such behavior cannot be accurately captured in a bond-slip behavior. The bond-slip behavior can only predict the average behavior along the bond length. In this study, only data from 50 mm from the loaded end of the bond length was considered, owing to the large variations in the damage propagation rate observed closer to the loaded end. Therefore, the model presented in this study is expected to yield conservative results. For a less conservative bond-slip model, much more experimental data is required. Therefore, to increase the accuracy of bond-slip models for fatigue cyclic loading, the constants in Eqs. 11 and 12 should be updated when additional data becomes available. Until then, the constants proposed in this study could be used as conservative values for the prediction of a damage propagation rate owing to fatigue cyclic loading.

![Fig. 14. The damage parameter versus number of fatigue loading cycles at different distances of the bond length (L in the legend means the length). (1.5 column)](image_url)

In addition to the exclusion of the data closer to the loaded end, the damage parameter variations in F-2 and F-4 showed violent variations; thus, approximately 60% of the data from those two tests were excluded in the calibration of the constants in Eqs. 11 and 12. The data from the F-3 specimen showed much less noise than the other two specimens; thus, most of the data from the F-3 specimen could be used to calibrate the constants in Eqs. 11 and 12.
5. Numerical Implementation

As previously presented, the CFRP-to-steel bonded interface was subjected to fatigue cyclic loading, and the applied force was used to control the test. Therefore, the numerical method used in modelling the quasi-static cyclic bond behavior is not efficient, as the governing equation (Eq. 3) was approximated by using the slip value at different locations. To overcome this disadvantage, the FDM proposed by Pietro [61], in which the CFRP axial forces and slip values are calculated at different locations, was modified by Zhou [43] to simulate the fatigue cyclic bond behaviors of the FRP-to-concrete bonded interfaces. With this method, the external force and slip boundary conditions at either the loaded end or far end could be applied, significantly improving the computational efficiency in the modelling of both the cyclic and monotonic loading cases. When simulating bond behavior under fatigue cyclic loading, the state of one point of the bonded interface is categorized into an unloading state and transition state. For example, when the number of loading cycles \(N\) is applied to the bonded interface, the unloading in the \(N^{th}\) loading cycles is condensed in one path from the maximum force to the minimum force. In this case, the point along the bonding length is labelled as the unloading state. Once the minimum force is reached, the point state is switched into the transition state. After \(M\) number of loading cycles, the stiffness is calculated based on the transition damage parameter in the previous step and the corresponding transition damage accumulation rate for \(M\) number of loading cycles. This process is repeated until the predetermined number of loading cycles is reached. For simplicity, the detailed algorithm is not presented here, and the readers are referred to Zhou [43] for the detailed algorithm.

Comparisons between the test results and simulation results from the proposed model are shown in Fig. 15. The test results only present the quasi-static loading results between fatigue cyclic loadings, whereas the numerical simulation shows the fatigue cyclic loading process (i.e., the quasi-static loading process is ignored in the current simulation). In specimen F-2 (Fig. 15a), within the first loading amplitude (i.e., 18 kN–22 kN), the failure of the bonded interface had already been observed in the numerical simulation. This is due to higher failure rate resulting from the calibrated constants in Eqs. 11 and 12. For specimen F-3, the predictions agreed well with the test results (Fig. 15b). The failure propagation rate was relatively higher than that from the experimental results; thus, the damaged bond length at the end of fatigue cyclic loading in the numerical predictions was higher than that from the experiments. This resulted in a lower ultimate load when loaded monotonically. The predictions for the F-4 specimen also showed a much higher damage propagation rate than the experiments (Fig. 15c).
The higher damage propagation rate shown in the predictions is expected, owing to exclusion of the data closer to the loaded end, which shows a much slower damage propagation rate. The good agreement of the predictions with specimen F-3, but significant under-prediction of the fatigue life in specimens F-2 and F-4, is believed to be owing to the bias of the data towards the specimen F-3, which was used to calibrate the constants in Eqs. 11 and 12. While the good agreement with the predictions of F-3 showed the accuracy of the model if sufficient data is available for calibration, the under-prediction of the results in F-2 and F-4 highlight the need for significantly additional accurate data to calibrate the model. Until additional data becomes available to determine better values for $\alpha$ and $\beta$, the equations proposed in this study can be used as conservative values for predicting the fatigue life of CFRP-to-steel bonded interface subjected to mode II loading. It is noted that this pioneering work is one of the first studies on fatigue behavior of FRP-to-steel joints under fatigue cyclic loading. There is a need for more studies in future to investigate the effect of stress ratio (i.e., $R$ ratio), loading frequency and variable amplitude loading on the fatigue behavior of the joints and also to count for the inherent scatter in the results of fatigue tests.
6. Conclusions

This study presents a series of single shear pull-off tests of FRP-to-steel bonded interfaces with different bond thicknesses under fatigue cyclic loading. Theoretical models are also presented to predict the behavior of CFRP-to-steel bonded interfaces under quasi-static cyclic and fatigue cyclic loading. The experimental results showed that the dominant failure mode of all of the specimens is the cohesion failure within the adhesive. Based on the experimental results, the following conclusions can be drawn:

(a) A comparison of the load-displacement curves shows that low-level fatigue cyclic loading (i.e., a maximum fatigue load less than 50% of the bond strength) does not affect the global stiffness. However, high-level fatigue cyclic loading (i.e., a maximum fatigue load more than 63% of the bond strength) causes a reduction in the stiffness.

(b) The high-level fatigue cyclic loading clearly showed damage propagation within the bonded interface. The fatigue propagation rate was shown to be dependent on the loading amplitude, as well as the number of fatigue loading cycles.

(c) The peak interfacial shear stress achieved during fatigue cyclic loading was found to be lower than the peak interfacial shear strength (i.e., the interfacial shear strength achieved during monotonic loading). The peak interfacial shear stress values of the fatigue cyclic loading specimens were found to be dependent on the maximum fatigue loading amplitude.

(d) The experimentally obtained bond-slip curves showed damaged plasticity behavior. In addition, once in the softening stage, the interfacial shear stresses become negative upon unloading to a zero load, indicating an interfacial shear stress reversal.

(e) The bond-slip curves also showed that, when subjected to constant amplitude fatigue cyclic loading, the interfacial shear stress reached a maximum, and then decreased monotonically. The area under the bond-slip curves of the fatigue-loaded interfaces remained smaller than that of the monotonically loaded bonded interfaces.

Based on the experimental observations and on existing theoretical models of CFRP-to-concrete bonded interfaces, theoretical models were developed for predicting the behavior of the CFRP-to-steel interfaces under fatigue cyclic loading. The key components of the theoretical models proposed in this study are the bond-slip curves under quasi-static cyclic and fatigue cyclic loading.

The damage propagation of the bond-slip models under quasi-static cyclic loading was defined as a function of the ratio between the dissipated energy ($\omega_d$) at a particular point and the total fracture energy ($G_f$). This function was obtained by a regression of the experimental data. The predictions from the proposed bond-slip model were found to be in excellent agreement with the experimental bond-slip curves.

Equations were also proposed for predicting the damage propagation rates of the unloading and transition curves of the bond-slip models during fatigue cyclic loading. The empirical constants of the proposed equations were obtained based on the experimental data. However, in obtaining the values of those constants, the effects of complex stress states near the loaded end were ignored, resulting in conservative predictions of the damage propagation rate.

The proposed model for predicting the behavior of CFRP-to-steel bonded interface was found to provide conservative predictions. With additional experimental data, the accuracy of the predictions could be improved. However, until such data becomes available, the proposed model could be used as a conservative model for predicting the behaviors of CFRP-to-steel bonded interfaces under fatigue cyclic loading.
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