BOND OF STEEL BARS ANCHORED IN STRAIN RESILIENT CEMENTITIOUS COMPOSITES

Souzana TASTANI¹, Konstantinos KATSIKAVELAS²

ABSTRACT

Strain resilient cementitious composites are engineered-design concretes made with fine-grained inorganic materials with embedded plastic fibers as mass reinforcement. This novel material can be an attractive alternative to brittle concrete because it encompasses significant strain deformation capacity (larger than 3%) as a result of the confining action introduced by the fiber dense network. Strain ductility may favor the construction in regards to: a) durability issues that engage any kind of cracking (autogenous or due to external actions) and b) effective design of structural elements by reducing geometry and required steel reinforcing ratios (longitudinal and transverse) for a preset performance limit state of the structure. Prerequisite for incorporating SRCCs in structural applications is to define the bond mechanism between bar reinforcement and surrounding media. This experimental study investigates the local bond of steel bars in SRCC under adverse tensile stress conditions. Parameters of investigation are the media (SRCC and its reference without fibers) and the bar diameter. The prevailing results are that the large deformation capacity of the SRCC enables higher bar strain development capacity and milder descend in the post-peak response of the bond-slip law as compared to the reference media (without fibers). The commensurate high bond toughness (i.e., the area under the bond-slip curve) occurs exclusively in the presence of confinement provided by the plastic fibers in arresting splitting along the bar embedded length. The increased bar strain development capacity is attributed to the associated high bond toughness; this dual effect indicates the potential for more efficient use of the reinforcement when the latter is embedded in SRCC in terms of shorter development length and exploitation of steel ductility reserves.

Keywords: bond toughness; slip; strain resilient cementitious composites; fibers; anchorage

1. INTRODUCTION

Strain resilient cementitious composites (SRCC) is a class of ECCs (Li, 1998) and are attempted for sustainable design of structures. They incorporate, apart from cement and fine sand, excessive amounts of by-products (usually fly ash from waste deposits of power plants) and high volumetric ratios (2%) of short plastic fibers (length in the order of 8-12mm) acting as randomly distributed mass reinforcement; the latter ingredient renders these materials with high deformation capacity and increased resistance against cracking, suitable for high-durability structural solutions. The deformability of SRCCs deviates from common ECCs as the tensile stress – strain response has a parabolic shape comparing to the apparent hardening of the ECCs. In SRCCs curve, both mild hardening and softening in a wide range of resilient deformation (more than 3%, Tastani and Savvidis 2017) up to a 15% loss of strength provide favorable conditions for sustained deformation capacity without severance at the cracks. The mild softening ends up with crack localization (Tastani et al. 2017) whereas in ECCs the hardening branch terminates with multiple cracking prior to localization into a single critical crack. The compressive stress – strain behavior of SRCCs is also parabolic due to restrain imposed by the fibers against lateral dilation; the peak stress occurs at strain near 0.005 and for 15% strength loss the associated strain is in the order of 0.008 (Georgiou and Pantazopoulou 2016, Zhou et al. 2015, Kesner et al. 2003).

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The incorporation of SRCCs in reinforced concrete (r.c.) structures attaches multiple benefits, namely resistance to environmental/corrosive attack due to the restrain against of cracking initiation and growth (i.e., reduction of steel rebar corrosion rates was reported by Ranade et al. 2016), reduction of the extensive amount of transverse reinforcement in strategic regions of the structure (Zhang et al. 2015) since the SRCCs are able to carry tensile stresses at high strains comparable to those undertaken by the reinforcement (with design limit of 2%) and even reduction of anchorage length and increase of the strain development capacity of the reinforcement anchorages (Eleftheriou et al. 2017) by favorably affecting the descending branch of the bond–slip law (Kanakubo and Hosoya 2014).

For the SRCCs to be effectively introduced in r.c. structures, prerequisite is the understanding of the bond mechanism between reinforcing bar and surrounding media. Bond controls not only the anchorage of longitudinal reinforcement but also the deformation of flexural r.c. members (Megalooikonomou et al. 2017). It is widely known that stress transfer between reinforcing bar and concrete is engaged through rib translation relative to concrete and comprises longitudinal bond stresses \( f_b \) and radial pressure \( \sigma_r \) (Figure 1). The radial pressure is equilibrated by hoop tension \( \sigma_{t,\text{hoop}} \) undertaken by the concrete cover. Due to limited tensile strength and deformability of concrete, critical implication is the occurrence of initially radial (as per the bar center) and finally longitudinal (as per the bar axis) splitting cracking in the concrete cover which abolishes the equilibrium and diminish the bond action. Additionally to the hoop tensile stresses, concrete is forced to carry longitudinal tension \( \sigma_{t,\text{long}} \) in Figure 1) due to moment imposed to the cross section of a flexural r.c. member; term \( \sigma_{t,\text{long}} \) is commonly ignored in the cross section analysis because it is practically diminished at the insignificant strain capacity of concrete (in the order of 0.015%). To this end, the replacement of conventional concrete with SRCC is expected to favorably affect both a) the bond in terms of strength and resilience to pullout slip, parameters that define the so-called bond toughness (i.e., the area enclosed between the bond–slip curve and the axis of slip) and b) the strain development capacity of reinforcement deeper in its hardening region given that the extreme concrete compression fiber of the cross section is not crushed but it is able to be deformed at higher strain.

A recent study by Georgiou et al. (2017) of direct tension pullout bond tests has shown that the bond strength can be doubled from 5.5 to 11MPa (for constant bonded length) when the cementitious composite cover, \( c \), becomes thicker (from \( c=1.2 \) to \( 2.8D_b \), \( D_b \) is the bar diameter). The reported bond strength can be accurately predicted by combining equilibrium between \( \sigma_r \) and \( \sigma_{t,\text{hoop}} \) (i.e. \( \sigma_r D_b=2c\sigma_{t,\text{hoop}} \) where \( \sigma_{t,\text{hoop}}=f_{t,\text{hoop}} \), \( f_{t,\text{hoop}} \) is the tensile strength of cover) and frictional model (i.e. \( f_b=2\mu\pi\sigma_{t,\text{hoop}} \), \( \mu \) is the frictional coefficient, \( \mu=0.9-1.2 \) for ribbed steel), both resulting in the expression \( f_b=2\mu\pi(2c/D_b)f_{t,\text{hoop}} \) by using values for \( \mu=1 \) (commonly assumed value) and \( f_{t,\text{hoop}}=3\text{MPa} \) (measured from associated dogbone tests). The associated slip values are accordingly increased from 2 to near 3 mm, magnitudes that are comparable to those obtained from bars in conventional concrete with excessive cover thickness (\( c=5D_b \), fib MC2010) and for pullout mode of failure. Interesting is that the descending branch of bond–slip law approaches the residual value (2 and 4MPa respectively, values that approximate 40% of bond strength in accordance with fib MC2010) at a slip equal to the...
bar diameter that coincides with the rib spacing (here $D_b=8$ and 10mm). The above suggest the feasibility of thinner SRCC cover in promoting a favorable pullout mode of failure as would occur in well confined anchorages.

This paper aims to experimentally investigate the state of local bond of steel bars developed in SRCC media under adverse tensile stress conditions, as those that occur when anchorage is developed in the tension zone of flexural members; in this case, the presence of curvature generates -additionally to the bond action- tensile stresses in the cover resulting in even lower bond magnitudes. In the experiment, local bond is developed over a short anchorage length (i.e., $L_b=5D_b$) occurring in the constant moment region (where no shear is present) of a four-point bending short beam. Parameters of investigation were the material structure (SRCC with 2% per volume polypropylene-PP fibers and the reference matrix without fibers) and bar diameter ($D_b=10$ and 12mm). Accompanying tests used to characterize the cementitious materials were also conducted (compression of cubes of side 100mm and 3-point bending of prisms 40x40x160mm). The beam test results are presented as curves of imposed load versus deflection illustrating all the benefits gained due to presence of PP fibers in the matrix, namely the increased strength and deformability. Both performance indices were imparted in the bond response as sustained bond strength up to large levels of controlled bar-slip; the elasto-plastic bond response occurred even without bar yielding. From inverse analysis of milestone points of load – deflection response, lower bound bond – slip curves were produced attempting to be used in design of steel reinforced SRCC structural elements.

2. EXPERIMENTAL PROGRAM

2.1 Materials: composition and properties

The composition of the SRCC matrix included (per weight): one part of Portland Cement 42.5, two parts of Greek calcareous fly ash (with sum of SiO$_2$, Al$_2$O$_3$ and Fe$_2$O$_3$ around 44%, CaO 40% and CaO$_f$ 14%, a by-product of a lignite-combustion power plant), 1.1 parts of fine silica sand (100% passage from sieve of size 0.5mm), 1.1 parts water (note that the ratio of effective water to binder is only $1/(1+2)=0.37$), 0.1 parts of superplasticizer (by Sika, Viscocrete Ultra 600) and 2% per volume PP fibers (by ThracePlastics, TMIX-12: $d_f=25\mu$m, $l_f=12$mm, $f_{fu}=400$MPa, $E_f=1.6$GPa, $\varepsilon_{fu}=0.25$, $\rho_f=0.91$gr/cm$^3$). The reference matrix -without fibers- was identical to that of SRCC, however lower amount of superplasticizer was needed. The purpose behind the use Greek fly ash was so as to classify this SRCC as “sustainable material” by addressing the problem of waste disposals and CO$_2$ emissions. Mean compressive strength from cubes (side 100mm) was found at 28 days as $f_{c,SRCC} \approx 40$MPa/$f_{c,reference} \approx 60$MPa whereas from inverse analysis of prisms 40x40x160 (mm) in 3-point bending the direct tensile strength was estimated as $f_{t,SRCC} \approx 3.6$MPa/$f_{t,reference} \approx 1.5$MPa (the associated peak loads were $P_{po,SRCC} \approx 2.5$kN/$P_{po,reference} \approx 1.5$kN). Interesting is that the fracture energy of the SRCC (the area under the load-deflection curve divided by the cross section area of prism) is extremely high as compared to the value of the reference matrix ($G_f^{SRCC}=2000$Nt·m$^{-1}$ and $G_f^{reference}=70$Nt·m$^{-1}$). The reinforcing bars used had diameter $D_b=10$mm and 12mm of steel S500 with yielding stress $f_{sy}=500$MPa.

2.2 Beam tests for bond assessment

A novel bond test setup is introduced for investigation of the local bond – slip characteristics of steel reinforcement in SRCC matrices. Twelve short beams of dimensions 370x80x70 mm (length x height x width) were casted, three identical coupons for each parametric combination (2 matrices x 2 bar sizes x 3 coupons). The beams were loaded under 4-point bending with the bonded length $L_b$ being developed in the half of the mid-span of the constant moment region, and also in the tensile region of the cross section (Figure 2a). Thus, both the matrix and bar were stressed in tension, succeeding in the objective to engage their tensile properties in developing the local bond – slip law. The bonded length was set equal to five times the bar diameter (i.e., $L_b=5D_b$, 50 and 60mm
depending on \( D_b \) of 10 and 12mm respectively) with a clear bottom cover \( c=2D_b \). The constant moment region was set equal to \( 2L_b=10D_b \). The single bar was fully bonded in the left shear span whereas it was covered by a sleeve along the right one aiming to eliminate the bond interaction in that region (Figures 2b,c). Because the moment gradient in the right shear span can’t be undertaken due to absence of bond, flexural in that region was achieved by using two auxiliary longitudinal bars of 8 mm (SS500 also). These bars were fully bonded along the right shear span and lap-spliced along the studied bonded length. The selected dimensions of specimens were chosen aiming to preclude all other modes of failure (flexure, shear) apart from bond failure which was the study objective. A preformed 20 mm wide groove was located just to the left of midspan of the beam, extending across the width and up to a distance of \( 2.5D_b \) from the tension face. The groove was necessary so as to control the magnitude of bar maximum tensile force which may be quantified through sectional analysis of the mid-cross section. Bond tests were conducted under displacement control with a loading rate of 0.2mm/min up to the attainment of the first peak and then of 0.5mm/min up to test termination.

3. EXPERIMENTAL RESULTS

The experimental behavior of each beam bond test is presented as load – deflection \( (P-\Delta) \) response; the deflection was measured at the points of imposed load aiming to associate them with the free-end slip of the anchorage. The specimens made with the reference matrix (without fibers) are denoted as R10 or R12 (depending on the bar diameter) while with SRCC as F10 or F12. Figure 3 presents the response of the reference specimens; it is mainly elastic up to peak load and then a sudden drop of load occurs due to the instant bond failure by splitting (either by side or bottom cracking). The R12 specimens undertook higher load (6kN) at lower deflection (around 0.6mm) as compared with the R10 specimens (5kN and 0.75mm).

Figures 4a,b depict the load – deflection histories of the bond tests made with SRCC. Clearly, the high
deformation capacity of the composite matrix was imparted to the response by sustaining the increased peak load up to large values of deflection. Intense flexural cracking was allowed due to the confining conditions introduced by the dense fiber network around the anchorage length. Failure by slowly descending response was associated with bottom splitting cracking or flexural cracking at the end of \( L_b \). Again, F12 specimens developed higher load but lower deformation capacity than F10 specimens. Note that the curves are presented up to the initial degradation of load that occurred at a chord rotation of \( \tan \theta = \Delta L_s / L_s = 4\text{-}5\% \) (\( L_s \) is the shear span length). This is because at larger deflection, \( \Delta \), the beam inclination due to the wide flexural crack located at the mid-length (at the right of the groove) results in alteration of the initial static system that was a simply supporting 4-point bending beam, and forces the left shear span to slide as per its support. This behavior indicates not-symmetrical response at advance state of deflection, and as such it is ignored in the following analysis.

Figure 4c summarizes the total experimental program by depicting the mean load-deflection curve for every subgroup of specimens; the mean curve was produced by averaging the peak and residual points in the case of the brittle R specimens whereas in the case of the ductile F specimens by averaging at every deflection level the three load values of each subgroup. In any case, the total deflection \( \Delta \) is the result of flexure \( \Delta^f \) and slip \( \Delta^s \) contributions; to this end in the next section of analysis these two contributions will be isolated aiming to derive the local bond – slip law. By comparing the mean responses of R and F specimens, noticeable are the alteration of behavior from brittle to ductile (achieved displacement ductility at least 5) and the increase of strength (almost double), both resulting in an enormous amount of absorbed energy in the case of the F specimens.

4. DERIVATION OF THE LOCAL BOND – SLIP LAW

In this section the SRCC-to-steel bar local bond stress – slip law is derived by implementing inverse analysis of the recorded experimental data as detailed in the following. The load versus deflection envelope of the bond specimen is approximated as a four-linear envelope (Figure 5a) where milestones are the end of elastic response (stage I: \( \Delta_1, P_1 \)), the strength (stage II: \( \Delta_2, P_2 \)) and the ultimate with sustaining of strength (stage III: \( \Delta_3, P_3 \)). Variation of deflection \( \Delta \) into the constant moment region is
the sum of flexural and pullout contributions, as $\Delta = \Delta^f + \Delta^{slip}$; considering that $\Delta$ was recorded only at the points of loading and that the total response is mainly slip-dominant, a simplification was made here by assuming that the recorded $\Delta$ is conservatively constant in the constant moment region (Figure 4b). To this end by implementing simple geometry between deflection $\Delta$ and rotation $\theta$ of shear span $L_s$, slip $s$ of reinforcement is calculated as:

$$s = \frac{2 \cdot h \cdot \sin(90 - \theta) - c}{\tan(90 - \theta)}$$  (1)
where \( h \) is the cross section height, \( \theta \) is the rotation of the shear span due to \( \Delta^{\text{slip}} = \Delta - \Delta^0 (\tan \theta = \Delta^{\text{slip}} / L_s) \), and \( c \) is the cover thickness up to the center of the bar \((c = 2.5D_b)\).

Term \( \Delta^0_i \) (1=1, 2-3 correspond to the aforementioned stages) and the associated \( P_i \) are related with the middle cross section curvature \( \phi_i \) and moment \( M_i \) through the principle of virtual work (Table 1). From sectional analysis the approximated load at yielding, \( P_y = \pi D_b^2 / 4 f_s \cdot 0.9 d / L_s = 31 \) and \( 44 \) kN for R/F10 and R/F12 respectively, is much higher than the attained peak load \( P_2 \) of any subgroup of specimens (i.e., for F10: \( L_s = 125 \) mm, \( D_b = 10 \) mm and \( d = 55 \) mm with peak load \( P_2 = 9.3 \) kN, for F12: \( L_s = 115 \) mm, \( D_b = 12 \) mm and \( d = 50 \) mm with peak load \( P_2 = 12 \) kN, Figure 4c). Therefore, the plateau of load – deflection response where no loss of load carrying capacity occurs with increased deflection, is not attributed to bar yielding but to the sustained lateral pressure exerted by the cover on the bar’s lateral surface, while the bar slips as per its surroundings. Thus, from stage II to stage III (Figure 5a), the bar stress and strain of the middle cross section is maintained constant (and lower than the yielding limit) and no curvature increase occurs; thus \( \phi_2 = \phi_3 \) and \( \Delta_2^{\text{slip}} = \Delta_3^{\text{slip}} \). The bond stresses up to stage II are exponentially distributed in \( L_b \) whereas in between stages II and III their distribution progressively becomes uniform with simultaneous bar debonding at both ends of splice (Tastani et al. 2015); the latter actually reduces the effective bonded length \( L_b \). Thus, the increase of total deflection from \( \Delta_2 \) to \( \Delta_3 \) is exclusively attributed to slip contribution \( \Delta^{\text{slip}} \). For the estimation of the bond stress at any stage, \( f_{b,i} \), uniform bond stress variation is simply assumed along \( L_b \); thus, its magnitude is related to the bar stress \( f_s \) through Equation 2.

\[
f_b = D_b / (4L_b) \cdot f_s \tag{2}
\]

In Equation 2 term \( f_s \) is approximated by performing sectional analysis with consideration of (i) force and moment equilibrium and (ii) Bernoulli’s hypothesis for plane cross section during bending (thus linear variation of strains occurs along the beam’s height). For the flexural analysis of components (i.e. calculation of compression stresses along the compression zone \( z \)), necessary is the compression stress–strain law for the SRCC \((\sigma_c-\epsilon_c, \text{Equation 3})\): the Hognestad’s parabola describes the ascending branch until the peak \((f_c, \epsilon_{co})\) whereas a plateau is postulated for strains exceeding \( \epsilon_{co} \) up to \( \epsilon_c = (1 + 0.15^{0.5}) \cdot \epsilon_{co} = 1.4 \epsilon_{co} \) (the latter is the result of implementing Equation 3 for \( \sigma_c = 0.85f_c \)). Performing the analysis, terms \( f_c \) and \( \epsilon_{co} \) for SRCC were set equal to 40MPa and 0.0045 whereas \( \epsilon_{co} = 0.0063 \).

\[
\sigma_c = \begin{cases} 
2 \epsilon_c / \epsilon_{co} - \left( \frac{\epsilon_c}{\epsilon_{co}} \right)^2, & \epsilon_c \leq \epsilon_{co} \\
f_c, & \epsilon_{co} < \epsilon_c \leq 1.4 \epsilon_{co}
\end{cases} \tag{3}
\]
In any of the two milestones (i.e., stages I and II, consider here the coincidence of stages II and III) of the $M-\phi$ diagram, the strain $\varepsilon_c$ of the extreme compression fiber of the cross section defines also the compression stress distribution along the depth of the compression zone $z$. In the case where $\varepsilon_c \leq \varepsilon_{co}$ the distribution of $\sigma_c$ along the $x$ is parabolic (as per Equation 3) whereas when $\varepsilon_c > \varepsilon_{co}$ the distribution is parabolic up to $\varepsilon_{co}$ (measuring with reference to the point of neutral axis) and it is followed by a uniform stress-block for $\varepsilon_{co} < \varepsilon_c \leq \varepsilon_{cu}$ (Figure 6a). Implementing force and moment equilibrium, the actual stress distribution along the height $h$ of the beam is replaced for convenience by two rectangular blocks, one referring to the tensile stress of size $f_{tave}$ (assumed value 2MPa, lower than $f_{ct}=3.6$MPa) where the accumulative tension force acts on the centroid (i.e., within $(h-z)/2$ from the neutral axis) and one referring to the compression stress of size $\alpha f_c$ and point of action of the compression force located at a distance $\beta$ from the neutral axis. Depending on the value of $\varepsilon_c$ with respect to $\varepsilon_{co}$, parameters $\alpha$ and $\beta$ are calculated by considering force equilibrium and moment equilibrium (i.e. $F_c=\int \alpha(\varepsilon_c) dx$, and $F_c=\int \alpha(\varepsilon_c) y dy$, where $y$ is defined with reference to the position of the neutral axis) as follows:

$$\varepsilon_c \leq \varepsilon_{co} \quad \alpha = \frac{\varepsilon_c}{\varepsilon_{co}} \left(1 - \frac{\varepsilon_c}{3\varepsilon_{co}}\right) \quad \beta = \frac{\varepsilon_{co} - 3\varepsilon_c}{4\varepsilon_{co} - \varepsilon_c}$$

$$\varepsilon_{co} < \varepsilon_c \leq \varepsilon_{cu} \quad \alpha = 1 - \frac{\varepsilon_{co}}{3\varepsilon_c} \quad \beta = \frac{6\varepsilon_c^2 - \varepsilon_{co}^2}{4\varepsilon_c(3\varepsilon_c - \varepsilon_{co})}$$

For the force equilibrium, the SRCC compressive and tensile forces are $F_c = \alpha f_c \cdot b \cdot z$ and $F_t = k \cdot f_{tave} \cdot b \cdot (d - x)$ respectively ($k=0.5$ for stage I, thus denoting triangular distribution and $k=1$ for stage II for uniform distribution of tensile stresses), while the bar tensile force is $F_b = A_p \cdot E_s \cdot \varepsilon_s$ (Figure 6a). The moment equilibrium is written as per the point of action of $F_c$. The resulting system of equilibrium (Equation 5) is implemented resulting in the calculation of the compression zone depth $z$ and the bar stress $f_t$; the latter through Equation 2 gives the estimation of the bond stress (parameter $\lambda$ defines the point of action of $F_t$; it is 2/3 for stage I and 0.5 for stage II).

$$F_c - (F_t + F_b) = 0$$

$$M = F_z \cdot (d - z + \beta) + F_t \cdot [\lambda \cdot (d - z) + \beta]$$

(5)
At stage III the actual compressive strain $\varepsilon_c$ of the extreme fiber is increased from the value $\varepsilon_c = \varepsilon_{c,2}^f$ due to sectional flexural curvature $\phi_2$ - from stage II- by an amount of $\delta \varepsilon_{c,\text{slip}}$ due to bar slippage (i.e. thus $\varepsilon_c = \varepsilon_{c,2}^f + \delta \varepsilon_{c,\text{slip}}$, Syntzirma et al. 2010). Bar slippage induces rotation of the critical cross section which is accommodated with decrease of the compressive zone depth $z$ (as it is calculated from flexural analysis at stage II) to a value $z'$ (Figure 6b). This is also confirmed by comparing the snapshots a-b of Figure 4a as per the penetration of flexural cracking deeper in the compression zone. This means that the compressive strength deposit exploits quicker due to bar slippage. For the definition of $z'$, given that the bar strain $\varepsilon_s$ is known from stage II term $\varepsilon_c$ is conservatively defined as the sum of the $\varepsilon_{c,2}^f$ and the strain increment $\delta \varepsilon_{c,\text{slip}}$; the latter is approximated as half of the slip $s$ from the previous stage II divided by the constant moment length - here equal to $2L_b = 10D_b$; thus $\varepsilon_c = \varepsilon_{c,2}^f + s/(20D_b)$. Note that the total slip is accumulated from both crack sides and defines the opening of the crack. Thus, only the slip that comes from the right side of the crack is considered here. Unknown of the Eqs. (4-5) are term $z'$ and coefficient $m$ that defines the tensile force resultant as: $m \cdot F_t = m \cdot k \cdot f_{st} \cdot b \cdot (d - x)$ where $k=1$ (also in Equation 5, $\lambda = 0.5$). For the reference specimens R10 and R12, the residual bond – slip stage after bond failure is associated with elastic bar unloading without sustained bar strain; to this end its curvature is calculated using the equations of stage I (see Table 1) for $P_{\text{cr}} < P_f$.

4.1 Analytical results

The analytical bond - slip laws of all specimens are presented in Figure 7 whereas the associated experimental $P-\Delta$ values used (from Figure 4c) and the calculated values of bar strain $\varepsilon_s$, extreme compression fiber $\varepsilon_c$ (or $\varepsilon_c$) and compression zone depth $z$ (or $z'$) are included in Table 2. The
adequate tensile strength but most importantly the high fracture energy of the SRCC matrices, as expected, were imparted in the bond response as sustained bond strength up to large levels of controlled bar slippage; the bond plateau occurs under constant bar strain, lower than the yielding limit (Table 2). Both bar sizes of the F specimens developed similar response up to bond strength (~7MPa) with the F12 being less ductile. The commensurate high bond toughness (i.e., the area under the bond-slip curve) occurs exclusively in the presence of confinement provided by the plastic fibers in arresting splitting along the bar embedded length $L_b$. From the analytical values of bar strain $\varepsilon_s$ (compare $\varepsilon_s$ values between F and R specimens in Table 2), it seems that the SRCC matrix increases the bar strain development capacity thus resulting in more efficient use of the reinforcement (i.e. $\varepsilon_{s,F12}/\varepsilon_{s,R10} =1.89$ and $\varepsilon_{s,F12}/\varepsilon_{s,R12} =2.13$). Note that the F-curves are deemed as lower bound bond–slip laws because the bond action and the presence of curvature along the $L_b$ compete for the tensile cover reserves. The R specimens (without fibers) resulted to a brittle bond–slip law with strength lower than half of the F specimens. Finally, the reduced values of compression zone depth $z'$ at stage III, as they were approximated by considering cross section rotation due to slip, are confirmed by the available snapshots of each F experiment (indicatively shown by Figure 4a-b).

Table 2. Experimental and analytical values in deriving bond law

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<th>$\Delta$ (mm)</th>
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5. CONCLUSIONS

The experimental program conducted in investigating the local bond of short steel bars embedded in strain resilient cementitious composites (SRCC) under adverse tensile stress conditions, as those that occur when anchorage is developed in the tension zone of flexural members, confirm the insight that all the benefits gained due to presence of PP fibers in the matrix, namely the increased strength and deformability, can be successfully imparted in the bond response as sustained bond strength up to large levels of controlled bar slippage. Such a ductile bond – slip law may result in increasing the bar strain development capacity given the anchorage length, or inversely, for an intended bar strain the engineer can accordingly regulate the anchorage length to lower values as compared with those obtained by using as matric conventional concrete. Given the similarity in size of the study variable ($D_b=10$ and $12$mm, values were chosen due to loading frame capacity limitations), the analytically approximated bond – slip curves almost coincide for both F and R specimens; to this end the research need to be extended by considering larger bar sizes with more intense rib profile which engage more of the surrounding SRCC media in tension.

6. REFERENCES


