Cyclic Loading of Glued-In FRP Rods in Timber: Experimental and Analytical Study

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Abstract

The axial load capacity and stiffness of Carbon Fibre-Reinforced Polymer (CFRP) and Glass Fibre-Reinforced Polymer (GFRP) rods glued-in timber is investigated under cyclic loading as the main design consideration for structures that experience load reversal (e.g. due to wind loading). Load cycles at 20, 40, 60 and 80% of the ultimate load and three repetitions per load cycle were considered. The main parameters examined are the effect of FRP rod, anchorage length and construction scenario. The construction scenarios represent full contact between timber faces, gaps in joints due to long-term effects (e.g. viscoelastic creep) and manufacturing tolerances, and contact with other materials. The GFRP rods exhibit 23% higher axial load capacity and 20% lower axial tensile stiffness than CFRP rods for an embedment length of 5*D*, where *D* is the diameter of the rod. The axial load capacity of the GFRP rods tends to plateau with increasing bonded length at anchorage lengths greater than 10*D*. Small gaps significantly decrease the axial compressive stiffness of the glued-in FRP rods at the first load cycles and the axial stiffness varies along the bonded length. An analytical methodology is presented to describe the bond stress transfer mechanism and the progressive bond degradation. The analytical tensile slip values agree fairly well with the experimental results when debonding takes place at 80% of the ultimate load.

Keywords: glued-in rods, FRP rods, timber, cyclic loading

Introduction

The need to reduce CO_2 emissions in the construction sector in light of the Paris agreement (Horowitz 2016) has resulted in a growing interest towards sustainable and renewable construction materials like timber. The variety of engineered wood products (e.g. glulam and cross laminated timber (CLT)) and building construction forms (e.g. post-and-beam frame and CLT platform) has resulted in different types of timber connections considering the different load transfer mechanisms and the anisotropic mechanical performance of wood. The most common timber connections include mechanical fasteners, as covered in Eurocode 5 (CEN 2004), but the use of adhesives with or without rods is also applicable. Glued-in rod timber connections exhibit high stiffness and axial load transmission at short embedment lengths, enhanced fire performance compared to dowel connections and can be aesthetically pleasing (Tlustochowicz et al. 2011). The use of Fibre-Reinforced Polymer (FRP) materials, such as Glass Fibre-Reinforced Polymer (GFRP) rods, can offer better chemical compatibility with adhesives (Madhoushi and Ansell 2004), enhance the durability performance of glued-in rod connections due to the corrosion-free nature of FRPs and improve their fire performance due to the lower thermal conductivity of FRPs compared with steel (Zhu et al. 2017). FRP rods are also promising for structural applications near electromagnetic fields (e.g. MRI rooms). Among different fibre types (e.g. carbon vs glass), GFRP rods are usually preferred due to their lower cost. However, Carbon Fibre-Reinforced Polymer (CFRP) rods exhibit higher elastic modulus, greater fatigue and creep resistance and better durability (Toumpanaki 2015).

Despite the considerable studies in glued-in steel rods (e.g. GIROD programme (SP Swedish National Testing and Research Institute 2002)), there seemed to be no consensus in the establishment of design methods for Eurocode 5 (CEN 2004). A new draft design proposal for Eurocode 5 is under preparation by the CEN/TC 250/SC5.T5/WG5 and the latest norm for glued-in steel rods, BS EN 17334:2021 (BSI 2021), includes testing standards, classification of adhesives, specimen manufacturing and design procedure (Annex A). Various design formulas have been proposed by researchers and national design guidelines for glued-in steel rods (for more details please refer to Stepinac et al. (2013)) which relate the axial load capacity to different parameters. Table 1 summarises selected design equations considered in this study. It is observed that the Riberholt equation (Riberholt 1988) includes a timber density parameter, whereas bar and moisture factors and the effect of edge distances are considered in the axial load capacity formula of the New Zealand design guidelines (NZTDS 2007). The GIROD design equation includes a material and geometrical factor as a function of the cross-sectional areas and the modulus of elasticity of the rod and wood. DIN 2010 (DIN 2010) proposes the simplest design formula where the bond strength is defined as a function of the bonded length. The effect of elastic modulus and the suitability of these design formulas for FRP rods has not been investigated due to the limited research on the effect of anchorage length in glued-in FRP rods. Variations in adhesive types and test methods (e.g. 'pull-compression' versus 'pull-pull' bond test method) result in contradictory experimental findings among studies and hinder further standardisation (Broughton and Hutchinson 2001; Mettem et al. 1999; Johansson and Bengtsson 2002). The 'pull-pull' bond test method, where the rod at one end is pulled under direct tension and the rod at the other end acts as anchorage (Fig. 1(a)), is the recommended test procedure for rods glued in timber in BS EN 17334:2021. However, the 'pull-compression' (defined in concrete applications as 'pull-out' test (Toumpanaki et al. 2018)) has also been applied due to its simplicity and ease of application for mechanical screening. In the 'pull-compression' method a steel frame reacts against the pull-out load and compressive stresses are applied at the loaded timber face (Fig. 1(b)). Finite element (FE) studies in glued-in steel rods have suggested that the 'pull-compression' test method results in lower axial load capacities with increasing bonded length (L_b>50 mm and $L_b/D_h>3$ where D_h is the hole diameter) compared to 'pull-pull' tests (Serrano 2004). Experimental studies showed that steel rods glued in glulam with epoxy adhesive exhibit higher load capacity with the 'pull-pull' test method at slenderness ratios, L_b/D_h=20, and similar capacity at L_b/D_h=10 irrespective of the test method (Johansson and Bengtsson 2002). This contradicts similar studies in reinforced concrete where the 'pull-compression' bond test yields higher bond strength values due to the applied compressive stresses that act as confinement and counteract the bearing stresses during pulling-out (Tastani and Pantazopoulou 2002; Toumpanaki 2015). The lower tensile and shear strength and elastic modulus of timber compared with concrete, and its anisotropic properties should also be considered to understand the differences in bond strength trends among different test methods and materials.

The effect of elastic modulus in the bond performance of glued-in rods has not been thoroughly understood. Fava et al. (2013) tested plane-weave textile CFRP and GFRP plates glued in glulam (GL24h) in a 'pull-compression' mode and recorded negligible bond strength deviations among FRPs at a bonded length equal to the plate width. At longer bonded lengths the GFRP plates failed by tensile rupture when loaded parallel to the grain. CFRPs exhibited consistently higher pull-out loads when glued perpendicular to the grain with a maximum increase up to 26% for a bonded length equivalent to three times the plate width. However, GFRP rods showed a 34% higher axial load capacity in (Titirla et al. 2019) (glulam GL24h) compared with CFRP rods at a bonded length of 15*D*, where *D* is the rod diameter. The differences in the reported bond strength trends among studies can be attributed to differences in the bond test method (‘pull-compression’ vs ‘pull-pull’ test method) and geometry of the FRP reinforcement (rectangular vs circular).

Different adhesive types have been investigated by several authors (Aicher et al. 1999; Mettem et al. 1999; Serrano 2001) with polyurethane and epoxy being the most suitable adhesives for glued-in rod connections. The preparation of glued-in rods is recommended to be conducted off-site by qualified technicians to ensure high quality control. However, defects can be introduced in adhesive joints even under strict control measures. The effect of defects in the bond strength and stiffness of steel rods glued in timber has been investigated by several authors (Ratsch et al. 2019; Gonzales et al. 2016; Johansson and Bengtsson 2002; Xu et al. 2020) with voids, incorrect mixing of the two-component adhesives and remaining sawdust in the timber holes exhibiting the most detrimental effects. Rod inclinations of up to 2% have been reported by Ogrizovic et al. (2018) in industrially manufactured glued-in steel rod timber specimens. Rod misalignment can occur either due to placement of the rod at an angle within the timber hole (lack of wire guide) or due to the drilling process. Misalignment of self-drilling screws has been extensively discussed in Trautz et al. (2016) and attributed to many parameters (e.g. natural growth characteristics along the drilling route, wood properties, technical equipment etc.). Gonzales et al. (2016) showed that the rod inclination (1-4°) or off-centring has a negligible effect in the axial withdrawal capacity of steel rods glued in glulam at anchorage lengths of 5, 10 and 20*D*. Xu et al. (2020) demonstrated via Finite Element (FE) studies that rod misalignment causes higher shear stresses at the loaded end at the thin adhesive region compared with the adhesive-rich region. However, the non-uniform shear stress distribution at the loaded end resulted in experimental axial withdrawal capacity and stiffness variations that were statistically insignificant. However, Gattesco et al. (2017) argued that misalignment of glued-in steel rods in glulam beam splice joints leads to longitudinal splitting and a decrease in the ductility performance of the joints. Rod misalignment can lead to imperfect contact between timber structural members at the assembly stage and introduction of initial gaps. Best construction practices should involve the filling of large gaps due to manufacturing tolerances with gap-filling adhesives as applied in the assembly of the timber gridshell roof in the Pods Sports Academy in Scunthorpe (Harris et al. 2012). Gaps in glued-in rod joints can be introduced due to long-term deformation accelerated by changes in the environmental conditions (temperature and relative humidity – *RH*). Shrinkage gaps can occur by a drop in the timber moisture content but these are more pronounced perpendicular to the grain (α_R=0.1371%/% and α_T=0.2525%/% (O’Ceallaigh et al. 2020)) compared with parallel to the grain (α_L=0.0122%/% (O'Ceallaigh et al. 2020)). The introduction of reinforcement restricts the unrestrained free shrinkage, but non-uniform deformation and imperfect contact can be derived between points of local reinforcement. Gaps in glued-in rod joints can be developed due to viscoelastic and mechano-sorptive creep deformation in both adhesive and timber. Verdet et al. (2017) conducted tensile creep tests in steel rods glued in glulam with epoxy adhesive at constant (20°C/65%RH and 50°C/72%RH) and variable environmental conditions. A 33% increase in slip values was recorded in specimens after 25 days at 20°C/65%RH and under 52% stress level. The slip values increased by fifteen times when the temperature was elevated close to the glass transition temperature of the adhesive (50°C/72%RH) at the same stress level. An increase in slip values has also been observed in GFRP rods glued in Laminated Veneer Lumber (LVL) with two epoxy types under 20% tensile creep load at 20°C/65%RH (Roseley et al. 2012). The increase in slip values due to long-term effects can greatly affect the compressive stiffness of glued-in rod joints under load reversal due to the lack of distributed compressive loading. Moreover, the compressive stiffness of glued-in rod joints at a discontinuity (e.g. node) can be considerably different. The compressive loading acts at the timber end face and a different bond stress mechanism is activated.

Most studies in connections with glued-in FRP rods have focused on static monotonic loading (Ling et al. 2018; De Lorenzis et al. 2005; Zhu et al. 2017) and there is limited research on the cyclic and fatigue performance of these joints (Madhoushi and Ansell 2004; Tannert et al. 2017). Existing research on glued-in rods under cyclic loading refers to the use of steel rods with emphasis on their ductility performance (Gattesco et al. 2017; Ogrizovic et al. 2018). Ogrizovic et al. (2018) carried out cyclic tests in glued-in steel rod connections with the 'pull-pull' test method and reported an insignificant decrease in the axial load capacity after cyclic loading. The cyclic loading regime followed the new proposal for the BS EN 12515 (BSI 2001) testing standards regarding the seismic evaluation of timber connections with three load cycles at 0.75, 1.0, 2.0, 3.0, 4.0 and 5.0 times the yield displacement and displacement rates at 0.01 and 0.1 mm/sec. In regions with low seismic hazard the performance of glued-in FRP rods under load reversal effects (e.g. wind uplift in timber roofs) is of interest for both new and strengthened existing timber structures. Moreover, there is limited reporting of the experimental axial stiffness of the glued-in rod connections, which is equally important when simulating a joint in a structures model. Ling et al. (2018) reported average axial tensile stiffness values of 42.8 and 41.6 kN/mm for GFRP rods (D=16mm) glued-in glulam with bonded lengths of 120 and 240 mm respectively after testing under monotonic loading. However, the stiffness values under compression are expected to be higher due to the composite action between the timber, adhesive and FRP rod and there is lack of knowledge about the effect of bond degradation in the axial compressive stiffness. The need of robust analytical models that can simulate bond degradation during loading is opportune. These models can provide a computationally less expensive tool to predict the axial withdrawal capacity and stiffness of glued-in FRP rods.

The aim of this study is to shed light on the effect of elastic modulus (CFRP vis-à -vis GFRP rods), bonded length and construction variations (e.g. absence of full contact) in the bond strength and stiffness of glued-in FRP rod connections under both tension and compression (cyclic loading). This further enables accurate modelling of glued-in FRP rod joints (e.g. splice joints) under service loading. CFRP and GFRP rods glued in timber with an epoxy adhesive are tested with the 'pull-pull' method under cyclic loading. The cyclic loading aimed at simulating load reversal effects in glued-in FRP rods at the serviceability limit state and close to the ultimate limit state. The performance of CFRP and GFRP rods is compared at a reference embedment length. The relationship between the anchorage length and the pull-out load is studied in GFRP rods and correlated with existing design formulas. Material viscous damping ratios are also proposed as calculated from the energy stored and dissipated at each load cycle. Particular emphasis is given to the axial stiffness of the glued-in FRP rods under both tension and compression. Three different construction scenarios are considered that can significantly affect the axial stiffness. In the first scenario small gaps are introduced between the timber contact faces due to long-term effects (e.g. viscoelastic creep deformation) or manufacturing tolerances. In the second scenario there is perfect contact between the timber faces in a glued-in rod connection and in the third case there is a discontinuous connection of a timber element with another material face (e.g. a concrete node). An analytical model is introduced for glued-in rods that defines the bond strength degradation along the bonded length and comparison between analytical and experimental slip values is made.

Experimental Procedure

Materials

Pultruded GFRP and CFRP rods (Sireg, Italy) with a core diameter of D=10 mm were used in this study (see Fig. 2). The FRP rods had the same resin matrix (vinylester) and fibre volume content (>65%) and were sand coated and helically wrapped with aramid fibres. Due to the external surface deformation, the outer apparent diameter of the GFRP and CFRP rods was D_o=11.1 mm and D_o= 10.7 mm respectively, as derived from the relevant cross-sectional areas, A_{ro}=97 mm^2 and A_{ro}=90 mm^2 , following the immersion method according to ACI 440.3R-12 (ACI 2012) guidelines (see Table 2). The CFRP rods had approximately three times higher longitudinal elastic modulus than the GFRP rods when the nominal values are compared (see Table 2). The experimental values of longitudinal tensile elastic modulus were 60.6 and 134.2 GPa for GFRP and CFRP rods respectively based on strain readings in the same type of FRP rods when tested with the 'pull-compression' method (Toumpanaki and Ramage 2018; Toumpanaki and Ramage 2021). The core cross-sectional area was adopted in the calculation of the tensile elastic modulus. The experimental elastic modulus of the GFRP rods is up to 32% higher than the nominal values. This is attributed to the different test method used to calculate the tensile elastic modulus ('pull-compression method'), the lower stress range employed (the rod was loaded up to 20% of ultimate load) and the lower number of specimens tested to derive the mean values.

A two-component thixotropic epoxy resin (Sikadur 30) was used as an adhesive. The tensile mechanical properties of the adhesive were experimentally measured according to BS EN ISO 527 (2012) (BSI 2019) using a 2 kN capacity Instron load cell. Five dumbbell specimens (170 mm (length) x 5 mm (thickness)) with a moisture content of 0.14% (as derived from ASTM D6980-17 (ASTM 2017)) were tested under tension at 1 mm/min. Two strain gauges attached in the longitudinal and transverse direction enabled the measurement of the Poisson's ratio. The experimental tensile Young's elastic modulus and tensile failure strength were 23% higher and 5% lower than the nominal values accordingly (see Table 2). Six specimens (68 mm (length) x 12 mm (width) x 5 mm (thickness)) with a 6 mm notch were tested under four point bending at 1 mm/min to determine the mode II stress intensity factor, $K_{II,}$ based on the test methodology in Ayatollahi et al. (2011). The moisture content of the specimens was 0.41% (ASTM D6980-17 (ASTM 2017)). The mechanical properties for both FRP rods (GFRP and CFRP) and the epoxy adhesive are summarised in Table 2.

The timber material was derived from a glulam panel made from spruce boards of C24 quality with edge and end laminations. The product was made in Stora Enso's Ybbs (Austria) factory. The mechanical properties of interest were those related to the local failure of timber along the bonded length. Therefore, the timber product was mechanically characterised based on BS 373:1957 (BSI 1957) and ASTM D143 (ASTM 2009) avoiding glue layers in the small specimens used. The timber specimens were stored at 20.9 ± 0.6 °C and *RH*=51.0 ± 2.0 % and had an average moisture content of 9% according to BS 373:1957 (BSI 1957).

Specimen Preparation

The geometry of the 'pull-pull' test specimens was based on recommendations in Aicher et al. (1999). The cross-section of the timber specimens was 70 mm x 70 mm and their length ranged from 180-510 mm depending on the bonded length under investigation. Three values of bonded length, L_b=50, 100 and 140 mm, were studied corresponding to 5*D*, 10*D* and 14*D* where *D* is the core diameter of the rod. Due to the low transverse compressive strength of the FRP rods, sleeve anchors filled with epoxy were prepared to pull-out the rods effectively. The length of the sleeve anchors varied from 90 to 210 mm (9*D*-21*D*) representing 1.8 times the relevant bonded length under investigation. Two concentric holes along the grain direction were drilled in the timber specimens at each end. At one end a steel rod was glued to serve as an anchorage and at the other end a FRP rod was glued within a hole diameter of D_h=16mm. The diameter of the drill hole is in accordance with the maximum value recommended in BS EN 17334:2021 (BSI 2021) for the rod diameter studied here. The casting of the epoxy was carried out horizontally through two small penetrations drilled perpendicular to grain direction. This method enables better quality control via filling of the second hole closer to the loaded end with adhesive that indicates evenly distributed resin along the bond-line and reduced void content (Tlustochowicz et al. 2011). The specimens were prepared in the lab and the drilling of the holes was carried out manually with a handhold electric powered drill and an alignment rig as depicted in Fig. 3(a). The holes were cleaned of wood fibre residues with compressed air. To ensure the alignment of the FRP rods during casting of the epoxy, acrylic rings were prepared for both the free and loaded end (see Fig. 3(c) and (d)). To avoid any longitudinal displacement of the rods during casting of the epoxy, the specimens were placed against a fixed timber block (see Fig. 3(b)). The specimens were stored at 19.0 ± 1.8°C and *RH*=64.8 ± 5.2% for at least 10 days before testing.

Experimental Programme

The bond strength of the glued in FRP rods was measured based on the 'pull-pull' test method. The test variables were the bonded length, the type of the FRP rod and the test regime. GFRP and CFRP rods were studied only for the 50 mm bonded length. This enabled comparison of bond strength values between 'pull-pull' and 'pull-compression' tests carried out in Toumpanaki and Ramage (2021) for the same bonded length (50 mm) and material parameters

(GFRP and CFRP rods, epoxy and timber). The experimental programme is summarised in Table 3. The specimen identification is *a_b_c_d* where *a* denotes the type of rod, G=GFRP and C=CFRP, *b* is the bonded length variable, *c* represents the loading regime (M=monotonic, CI= cyclic loading regime I, CII=cyclic loading regime II and CIII=cyclic loading regime III) and *d* is the specimen number. The group identification is based on *a_b* where *a* is the FRP rod type and *b* is the bonded length. The experimental programme was defined based on the available number of specimens. The main focus was the effect of cyclic loading on the axial withdrawal capacity and stiffness of glued-in FRP rods considering representative scenarios of full contact either with another material (e.g. concrete node-cyclic loading regime III) or with timber (cyclic loading regime II). Therefore, the higher number of specimens is allocated in these cyclic loading regimes. To acknowledge the effect of gaps in the stiffness of glued-in FRP rods, one specimen from the G_100 and G_140 group was considered appropriate to indicate variations in the stiffness between different construction case scenarios. The cyclic loading regime was based on the failure loads of specimens under monotonic tensile loading. For the C_50 and G_50 groups the reference failure load ($F_{rult,mon}$ =15.9 kN and 14.2 kN respectively) was derived from similar specimens tested with the 'pull-compression' method (Toumpanaki and Ramage 2021). A deviation of up to 4.1% in the pull-out failure loads for L_b/D<10 was initially expected between the two different experimental methods as reported by Gustafsson and Serrano (2002) for steel rods glued-in glulam. It was assumed that the same load capacity variations apply in glued-in FRP rods. One specimen from each G_100 and G_140 group was tested under monotonic tensile loading to define the reference load for the cyclic loading regime. Three cycles were adopted in the G_50 and C_50 specimens and four cycles in the G_100 and G_140. The stress levels of the cyclic loading were selected to represent service loading (20 and 40%) and proximity to failure load (60 and 80%). Each cycle consisted of three load (tension)-unload-reload (compression)-unload repetitions. The specimens were loaded to failure after the completion of the last cycle.

Three different test configurations were adopted (Cyclic I, II and III), as depicted in Fig. 4, based on different construction scenarios. The cyclic scenario I (Fig. 4(a)) is representative of long-term effects (e.g. creep) and manufacturing tolerances/defects where small gaps between the joint timber elements can occur and in the cyclic scenario II (Fig. 4(b)) full contact between the timber faces in a glued-in rod connection is assumed. The cyclic scenario III is indicative of a glued-in rod connection between timber and other materials (e.g. at a steel or concrete node) where compressive stresses are applied directly at the timber face with engagement of the rod via bond forces.

The main research area of focus was cyclic loading tests under a full contact scenario and one specimen from the G_100 and G_140 groups was considered to indicate variations between cyclic loading I and II.

The tests were carried out in an Instron test frame with a 150 kN load cell capacity at a displacement rate of 0.5 mm/min at both loading and unloading satisfying the speed recommendations of ACI 440 guidelines for bond test methods (ACI 2001). The specimens were loaded in both tension and compression via a steel frame fitted to the Instron crosshead (see Fig. 5(a)). Any minor misalignments were corrected with the use of plaster sheathed in a plastic bag such that full contact was attained. The slip values were recorded at the loaded end (x=0 mm) and at a distance of x=50 mm and x=100 mm from the loaded face in the G_100 and G_140 specimens respectively (see Fig. 5(b)). Two linear variable differential transformers (LVDTs) for each reference measurement (as shown in Fig. 5(b)) were used. The FRP rod extension was subtracted from the slip values at the loaded end based on Eq. (1).

$$sl = \mathrm{LVDTs}(x=0\ \mathrm{mm}) - \left(F_r / (E_r A_r)\right) L_{un} \quad (1)$$

where sl is the loaded end slip value, LVDTs(x=0mm) indicates the average reading of the LVDTs at the loaded end, F_r is the axial load, E_r is the experimental tensile Young's modulus of the FRP rod, A_r is the core rod cross-sectional area and L_{un} is the free unbonded length of the FRP rod (see Fig. 4(c))

Shims were used between the anchorage and the steel frame at both ends in the cyclic load scenario II to the ensure both the FRP rod and the timber face are loaded at the onset of testing (Fig. 4(b)). In the cyclic load scenario I small gaps of 0.5 mm (G_140) and 0.8 mm (G_100) were predefined with the aid of appropriate shims (Fig. 4(a)). To calculate the gap in the G_100 group, an increase in slip with a viscoelastic creep factor of 1.33 was assumed, as derived from Verdet et al. (2017) for glued-in rods loaded at 50% of failure load, and a construction gap of 0.6 mm due to an average 1° misalignment was superposed. The gap in the G_140 group was derived from the viscoelastic creep deformation alone adopting the same creep factor in the G_100 group. In both groups the instantaneous slip was subtracted from the final slip values assuming a load reversal scenario where there is no sufficient time for creep recovery. Deviations in the creep factor due to differences in the epoxy formulation should be considered between the current and Verdet et al. (2017) study. The moisture content of the specimens at testing was 8.7% as calculated according to BS 373:1957 (BSI 1957).

Some specimens exhibited a rod misalignment of up to 2.6%. One specimen in the G_100 group and 2 specimens in the G_140 group exhibited rod inclinations in the range of 1-2°. This was more pronounced in the longer specimens. Rod inclination was judged to be related to the speed of drilling and accumulation of small tolerances during the test specimen preparation (e.g. inner diameter of acrylic rings). The observed rod misalignment is expected to have a low impact on the axial withdrawal capacity of the specimens as discussed previously.

Results and Discussion

A summary of the experimental results, the failure load, the bond strength at the rod/adhesive (τ_{ra}) and wood/adhesive interface (τ_{wa}), the loaded end slip at failure, the secant stiffness at both the serviceability and ultimate limit state and the failure modes are presented in Table 4. The τ_{wa} and τ_{ra} bond strength represent average shear stress values as derived from the failure load divided by the hole and rod surface area respectively (uniform bond stress distribution assumption). A non-uniform bond stress distribution is expected at longer bonded lengths (e.g. L_b=100 and 140mm) due to progressive bond degradation and the average values represent an estimate of the actual bond strength. The results will be discussed next in separate sections.

Failure loads and bond strength values

The ultimate failure loads in each group are depicted in Fig. 6(a) according to the type of loading. The bond strength values at the wood/adhesive interface are shown in Fig. 6(b) for each group. The glued-in GFRP rods exhibit on average a 23% higher axial load capacity than the CFRP rods when specimens with a 50 mm bonded length are compared. The same FRP rods when tested in a 'pull-compression' mode glued in the same timber material with the same epoxy had shown smaller bond strength relative differences and exhibited lower bond performance (Toumpanaki and Ramage 2021). The average bond strength of the GFRP and CFRP rods was 6.18 and 5.80 MPa respectively after cyclic loading (Toumpanaki and Ramage 2021). An average 30% decrease in the bond strength of FRP rods glued in timber is observed when the 'pull-compression' method is adopted. The compressive stresses from the reaction plate in the 'pull-compression' method counteract the bond bearing stresses developed from the mechanical interlocking effect and the bond strength differences among FRP rods and surface profiles are minimised. The different surface profiles of the FRP rods (mechanical interlocking effect) affect more the pull-out loads compared with the different stiffness (E_rA_r) values. This has also been highlighted by De Lorenzis and Nanni (2002) for NSM FRP systems in concrete. The GFRP rods had a greater bulging effect due to the helical wrapping exhibiting greater mechanical interlocking during bond testing. As one might expect, a rise in the pull-out failure load is observed with increasing bonded length. The relationship between pull-out load and bonded length tends to plateau at bonded lengths higher than 10*D* where a 40% increase in the embedment length results in a 10% increase in the axial load resistance when results at bonded lengths of 10*D* and 14*D* are compared. The average bond strength values of the GFRP rods decrease with increasing bonded length with a 39% drop being recorded at a 180% increase in the embedment length. This non-linear increase in axial load resistance with increasing bonded length is attributed to the progressive bond failure mechanism taking place at higher bond lengths before ultimate failure. This is studied with the analytical modelling. The G_100_CII_3 specimen failed at the anchorage end and it is not included in the data of Fig. 6. The G_100_M_1 specimen showed 22% lower axial load capacity than the specimens tested after cyclic loading. Visual inspection of the failure interface in the G_100_M_1 specimen showed that the wood/adhesive interfacial failure shifted to a resin/rod failure at approximately 10 mm from the free end. This is attributed to microvoids in combination with peak shear stresses at the free end leading to crack propagation and failure at lower load. Residual stress relief and redistribution can take place after cyclic loading leading to higher ultimate load capacity (Johansson and Bengtsson 2002). Internal stresses can develop due to shrinkage effects from epoxy curing and environmental conditions. However, the pull-out resistance after monotonic and cyclic loading was similar in the G_140 group and residual stress relief is considered negligible. The number of specimens is not statistically significant and firm conclusions cannot be derived.

Bond failure modes

The majority of bond failures occurred at the wood/adhesive interface (W/A) irrespective of the bonded length (Fig. 7(a)). Four specimens exhibited a mixed type of failure where the wood/resin interface failure was followed by a wood plug type of failure (Fig.7(b)). Pure rod/adhesive (R/A) failure modes were observed in the C_50 group (Fig. 7(c)). This is attributed to the surface deformation of the CFRP rods. Both FRP rods had the same helically wrapping system at the outer surface but GFRP rods showed greater extrusions resulting in a higher apparent outer diameter, as discussed previously. Therefore, the mechanical interlocking effect in the bond mechanism is expected to be greater in GFRP rods and shearing off of the external outer layer is favoured in CFRP rods, as experimentally observed. Similar failure modes in CFRP rods were observed in Corradi et al. (2015) for near surface mounted systems in chestnut and fir specimens and in Toumpanaki and Ramage (2021) in block laminated spruce timber specimens tested with the 'pull-compression' method.

Splitting failure modes were also observed. In the G_50_CIII_1 and G_140_M_1 specimens splitting was related to the presence of a knot at the loaded end (see Fig. 7(d)) and in the G_140_CII_1 specimen a shrinkage splitting crack was observed at the loaded face before testing. Yet, splitting as part of a mixed failure mode does not discount the axial load resistance of the specimens. Splitting failure modes at long embedment lengths (>=180 mm=11.25*D*) were also observed in ribbed GFRP rods glued in glulam blocks with a two component epoxy adhesive (Ling et al. 2018). The recommended minimum edge distances of 2.5*D* (DIN 2010) for glued-in steel rods should be revised for FRP rods to avoid splitting failure modes. Radial cracks in the resin layer extending occasionally within the wood layer were mostly observed in G_100 and G_140 groups (Fig. 7(a) and (d)). The presence of these cracks is indicative of the bond mechanical interlocking mechanism in GFRP rods. Tepfers (1998) has shown that bond in sand coated GFRP rods embedded in a concrete ring acts at an angle of 30° resulting in the development of hoop stresses (thick-walled analysis). However, the thick-walled analysis may be applicable in the adhesive layer but deviations from a radial cracking pattern can be expected in timber due to its anisotropic properties.

Secant stiffness

The secant stiffness of all specimens at the final stage of loading up to failure for the both the serviceability (SLS) and ultimate limit state (ULS) are summarised in Table 4. The stiffness at the serviceability state was based on the slip values at the 10% and 40% of the ultimate failure load, where structures are mostly expected to be loaded during their design life. The stiffness at the ultimate limit state is the secant modulus of the load-displacement curve at failure.

The C_50 group exhibited on average 23% and 27% higher stiffness than the G_50 group at the SLS and ULS respectively. This is attributed to the higher elastic modulus of CFRP rods. The stiffness of the GFRP rods glued-in timber tends to slightly decrease with increasing bonded length. This is attributed to the progressive bond degradation taking place near the loaded end at longer embedment lengths. Therefore, the difference in the axial tensile stiffness between the G_100 and G_140 group is insignificant (within one standard deviation). A decreasing trend in the tensile stiffness of GFRP rods glued-in glulam has been observed in Ling et al. (2018) for bonded lengths ranging from 120 mm to 240 mm. Glued-in GFRP rods with a bonded length of 50 mm tested in Toumpanaki and Ramage (2021) with the 'pull-compression' method exhibited an average tensile stiffness of 74 kN/mm and 53.4 kN/mm at SLS and ULS respectively. The increase in stiffness reported in the 'pull-compression' method is attributed to the minimisation of the shear lag effect from the reaction plate.

To understand the effect of any gaps in the stiffness of glued-in GFRP rod connections, the pull-out load versus loaded end slip values have been plotted in Fig. 8 for the G_100_CI_1 and G_140_CI_1 specimens (cycling regime I). The G_140_CII_3 and G_100_CII_2 are indicatively selected from the cycling regime II for ease of comparison. The axial compressive and tensile stiffness at the loaded end had similar values in the cycle 1 of the G_140_CI_1 specimen and in the cycles 1 and 2 of the G_100_CI_1 specimen. The slip values under compression in the cyclic regime I derive from the contraction of the GFRP rod in addition to any relative slip between timber and rod. It should be noted that the slip values under compression have been corrected for the rod's extension due to the free unbonded length apart from the initial gap under consideration. In the cyclic regime I high slip values are recorded under compression up to the point where the slip is equal to the initial gap and full contact is achieved. When full contact is attained (e.g. above cycle 2 and cycle 3 in the G_140_CI_1 and G_100_CI_1 specimens respectively) the effective axial compressive stiffness is up to 2.7 and 1.7 higher than the respective tensile stiffness in the G_140_CI_1 and G_100_CI_1 specimens accordingly.

The secant stiffness of the G_100 and G_140 groups along the bonded length is depicted in Fig. 9 at every load cycle under both compressive and tensile loading for the cycling regime II. The stiffness values are plotted with respect to the mid-point of the measurement distance (e.g. x=75mm for readings between '50-100' mm) since the LVDTs recorded average relative deformations between measurement points. For clarity the axial stiffness values at the loaded end (x=0) are depicted in Fig. 10 under both tension and compression. The axial tensile stiffness at the loaded end is much lower than the relevant stiffness values along the bonded length as expected. The tensile stiffness at the loaded end relies on the relative slip between the rod and the timber end face whereas the relative slip values along the bonded length are derived from the timber deformation between measurement points (Fig. 11). In all groups both the axial tensile and compressive stiffness increase with increasing load cycle irrespective of the measurement point. In both the G_100 and G_140 specimens there is a sharp increase in the axial tensile stiffness at x=75 mm above load cycle 3 (Fig. 9(a)), whereas the tensile stiffness at the loaded end (x=0) remains constant. This is attributed to local debonding near the loaded end. Due to the lower resulting bond stresses, there is a reduction in the tensile stresses transferred to the timber leading to lower timber axial deformation (at x=75mm) and higher stiffness. In the G_140_group higher tensile stresses and timber deformation is expected at the free end due to the composite action resulting in a drop in the axial tensile stiffness. This is studied further with the analytical modelling. It should be noted that the applied load in the fourth cycle of the G_140 group is close to the axial withdrawal capacity (80% $F_{ult,mon}$) and bond degradation is more likely to occur. The axial compressive stiffness is similar along the bonded length in the G_140 group and it is approximately 61% lower than the axial tensile stiffness at x=75 mm (Cycle 1-2). Under compression higher timber deformation is expected due to the uniform loading. Under tension an effective timber cross-sectional area is activated to carry the applied load (tension stiffening mechanism) due to the bond stress transfer mechanism (see Fig. 11). The compressive axial stiffness at the loaded end is lower (5-22%) than the values reported along the bonded length (see Fig. 9(b)). This is attributed to the lower elastic modulus of the crushing region developed at the loaded timber end face. Cepelka and Malo (2016) studied the deformation characteristics of glulam (GL30c) end joints under compression and reported stiffness values in the range of 135-185 kN/mm. Similar compressive stiffness is recorded here at the loaded end but differences in the timber grade and specimen geometry should also be considered. Violation of the composite action and relative slip between materials is expected within the crushing region.

Fig. 12 shows the axial stiffness values for the C_50 and G_50 group at each load cycle under both tension and compression (cyclic regime III). There is an apparent increase in axial stiffness in both tension and compression with increasing load cycle attributed to viscoelastic deformation and wood crushing during the compressive cycle. Under compression the stiffness in the C_50 group is lower than the G_50 group. The differences in the elastic modulus between the FRP rods should play a negligible effect in the compressive axial stiffness. Assuming full composite action the theoretical contribution of the CFRP and GFRP rod in the elastic stiffness is 15% and 6% respectively. The lower compressive stiffness in the C_50 group indicates local debonding as discussed previously. Differences in the reference load at each load cycle between FRP rods should also be considered in terms of local debonding effects (refer to Table 3). The compressive axial stiffness in the G_50 group of the scenario III is higher than the G_100 and G_140 groups of the scenario II.

Material viscous damping ratio

Under cyclic loading at the serviceability limit state, the energy absorption and thus the damping of the glued-in rod connections is expected to be low compared with timber dowel connections where friction losses additionally take place. In glued-in rod connections the energy dissipation relies on the creep deformation and hysteretic behaviour of the materials (FRP rod, adhesive, timber). At a given load cycle the energy absorption decreases with increasing load-unload repetitions since viscoelastic creep deformation occurs at first loading. This is shown in Fig. 13 with a pull-out load versus loaded end slip plot for a typical cyclic loading regime.

Table 5 summarises the damping ratios for each group and at each load cycle. The damping ratios are calculated from Eq. (2).

$$\xi = E_p / 2\pi \cdot E_{storage} \qquad (2)$$

where $E_{storage}$ is the energy stored during the first loading of the cycle and E_p=$E_{storage}$-E_e, where E_p is the energy dissipation and E_e is the elastic energy stored during load-unload cycles. The relevant energy definitions can be visualised in Fig. 13.

The damping ratios were derived accounting for the energy dissipation under both compression and tension. It is observed that the material viscous damping ratio decreases with increasing load (load cycle) irrespective of the type of FRP rod. There is no clear relationship between the bonded length and the damping ratio. The CFRP rods exhibit the lowest damping ratios.

Effect of bonded length

To understand the effect of bonded length in the pull-out load, the experimental values versus the slenderness ratio, L_b/D_h, and the hole surface area, L_{ph}, have been plotted in Fig. 14(a) and 14(b) respectively. Experimental data as found in literature for both CFRP (De Lorenzis et al. 2005) and GFRP (Zhu et al. 2017; Ling et al. 2018) rods have also been considered in the plots. In De Lorenzis et al. (2005) the 'pull-compression' test method was adopted.

In the current study the increase in the axial load capacity with increasing bonded length is limited for L_b/D_h>6. In Zhu et al. (2017) the plateau in the axial load capacity with increasing slenderness is related to the rod diameter and it is more apparent for the lowest diameter, D=9.5 mm. However, the pull-out load increases linearly with L_b/D_h in Ling et al. (2018) and De Lorenzis et al. (2005). The non-linear trends in both the axial load capacity and bond shear strength with respect to the slenderness ratio are related to the non-uniform bond stress distribution and progressive debonding at higher embedment lengths. The bond shear strength decreases with increasing L_b/D_h ratio except in De Lorenzis et al. (2005), as shown in Fig. 14(c). The drop in τ_{wa} is steeper in the experimental data presented here. The adhesive modulus in Ling et al. (2018), De Lorenzis et al. (2005) and Zhu et al. (2017) was in the range of 1180-2800 MPa and an adhesive with a much higher elastic modulus (see Table 2) is adopted in the current study. In all studies an epoxy adhesive was adopted but in Zhu et al. (2017) where polyurethane (PUR) was applied. The timber tensile elastic modulus parallel to the grain varied from 8.4-12.8 GPa. The axial load capacities according to the design guidelines tabulated in Table 1 are presented in Fig. 14(d). In Fig. 14(d) experimental results from literature with similar rod or hole diameter to the current study are included for ease of comparison. The New Zealand guidelines (NZTDS 2007) predict well the experimental pull-out loads reported here but do not yield a 'plateau' in the load capacity with increasing bonded length. The pull-out load data from Zhu et al. (2017) for the GFRP rods with a diameter of D=12.7 mm fits well with the design equations in DIN (DIN 2010) and Riberholt (1988). Yet, it should be noted that a reference diameter of D=10 mm and the epoxy strength factor was considered in the aforementioned design formulas (see Table 1). In Zhu et al. (2017) a PUR adhesive was adopted and a different shear strength factor should be applied (e.g. a higher bond factor according to Riberholt (1988)). There are different trends depending on the adhesive type, rod diameter and bonded length that cannot be captured accurately by the current design guidelines. The epoxy bond strength, τ, and bond fracture energy, G, in the GIROD design formula were derived from the relevant experimental data as proposed by Gustafsson and Serrano (2002) in lieu of the relevant factors developed for steel rods and thin glue-line thicknesses. The GIROD design formula demonstrates a non-linear relationship between the pull-out load and the slenderness ratio. Yet, it consistently underestimates the load capacity acting on the safe side. The purpose of the current study is not to propose another design formula by including fitting parameters. The GIROD design formula reflects better trends and material variations and it is based on fracture mechanics theory. Deviations due to differences in adhesive properties and bond test methods are expected. More experimental data is needed on glued-in GFRP rods and the effect of different adhesives should be studied in relationship to a theoretical based design formula.

Analytical modelling

Analytical models

Two main analytical approaches are adopted in literature to model the bond stress distribution along the bonded length in glued-in rod connections or in near surface mounted systems (e.g. (De Lorenzis and Nanni 2002)) where the same principles apply. Bond stress-slip models, as applied in reinforced concrete structures ,and the Volkersen model, as adopted in single- and double-lap adhesive joints, are commonly considered.

Bond stress-slip models

Bond stress-slip models are commonly derived from the experimental data (pull-out load versus slip plots) of bond tests by using curve fitting tools and assuming a uniform bond stress distribution along the bonded length. This is more accurate for small embedment lengths (2-3*D*) (Pecce et al. 2001; Toumpanaki and Ramage 2018). The bond stress is a function of the relative slip between the rod and the timber element and strain/displacement compatibility at the rod/adhesive and timber/adhesive interfaces can be violated. The slip can be defined based on Eq. (3)

$$\frac{dsl}{dx}=\varepsilon_r-\varepsilon_w \tag{3}$$

where ε_r and ε_w are the rod strain and average timber strain respectively of an element *dx*.

The bond stress is related to the slip based on Eq. (4)

$$\frac{\mathrm{d}^2 s_l}{\mathrm{d}x^2}=\frac{L_{pr}}{A_r}\left(\frac{1}{E_r}+\frac{1}{E_w A_w}\right)\tau \tag{4}$$

where $\tau=f(sl)$ represents a bond stress-slip model as shown in Fig. 15(d), L_{pr} is the rod perimeter, E_r and E_w are the Young's Elastic modulus of the rod and timber respectively and A_r and A_w are the cross-sectional area of the rod and timber respectively. In this analytical approach, the adhesive shear strain is not considered. More details on the derivation of Eq. (3) and (4) can be found in (Muhamad et al. 2012).

Volkersen model

The analytical approach for adhesive joints, as first introduced by Volkersen (1938), accounts for the shear strain in the adhesive layer based on displacement compatibility at the rod/adhesive and wood/adhesive interface. Therefore, full composite action between the materials is satisfied and the shear stress equation is given by Eq. (5)

$$\tau=G_a\gamma_a(x)=G_a\left(\frac{u_a(r_o,x)-u_a(r_1,x)}{t_a}\right) \tag{5}$$

where G_a is the shear modulus of the adhesive, t_a is the adhesive's thickness and $u_a(r_o,x)$ and $u_a(r_1,x)$ are the displacements of the adhesive at r=r_o (rod/adhesive interface) and r=r_1 (wood/adhesive interface) respectively. Based on the equilibrium conditions in a two-dimensional analysis for a glued-in rod joint, the second derivative of the bond stress can be expressed in terms of shear stress with Eq. (6) and the general solution of the differential equation can be directly derived.

$$\frac{\mathrm{d}^2\tau}{\mathrm{d}x^2}=\frac{G_a}{t_a}\left(\frac{L_{pr}}{E_rA_r}+\frac{\pi D_h}{E_wA_w}\right)\tau(x) \tag{6}$$

Basic assumptions of the Volkersen model are that the adhesive's axial deformation is negligible and the through thickness shear stress is considered constant (da Silva et al. 2009).

Fu et al. (2000) model

A model that accounts for the different bond shear stresses developed between materials in a composite medium was introduced by Fu et al. (2000) and applied in the pull-out stress transfer mechanism of a fiber in a composite material. This model is considered here to simulate the bond at the rod/adhesive and wood/adhesive interface (see Fig. 15(a)). In the model by Fu et al. (2000) the axial and shear deformation of the adhesive are considered and this is more representative of a glued-in FRP joint with a stiff adhesive (current study). The through thickness shear stresses in a composite medium are expressed in a Lamè form.

$$\tau_w(r,x)=\frac{p_w}{r}+q_wr \tag{7}$$

$$\tau_a(r,x)=\frac{p_a}{r}+q_ar \tag{8}$$

where τ_a and τ_w are the shear stresses in the adhesive and wood respectively, r is the radial coordinate with respect to the centroid of the rod and p_a, q_a, p_w and q_w are constants to be determined.

By satisfying the relevant strain compatibility and boundary conditions at the interfaces, the analytical solutions for the axial stress distribution of the rod, adhesive and wood and the relevant shear stress distribution along the bonded length can be derived. The shear stress distribution at rod/adhesive and wood/adhesive interface are given by Eq. (9) and (10). More details on the derivation of the equations and constant values can be found in Fu et al. (2000).

$$\tau_{ra}(x)=\frac{r_o}{2}\beta[C_{21}\cosh(\beta x)+C_{22}\sinh(\beta x)] \tag{9}$$

$$\tau_{wa}(x)=-\frac{r_2^2-r_1^2}{2r_1}[B_{26}\beta C_{21}\cosh(\beta x)+B_{26}\beta C_{22}\sinh(\beta x)] \tag{10}$$

where r_o, r_1 and r_2 are the radial coordinates of the rod, adhesive and wood respectively at the outer boundary,

$C_{21}=\frac{\sigma_{ro}\left[1-\frac{r_o^2}{B_{15}}+r_o^2\cosh(\beta L_b)/B_{15}\right]}{\sinh(\beta L_b)}$, $C_{22}=-\frac{r_o^2\sigma_{ro}}{B_{15}}$, σ_{ro} is the axial stress of the GFRP rod at the loaded end and β, B_{26} and B_{15} are coefficients as a function of the elastic material properties and geometric factors of the glued-in rod joints and additional coefficients defined in Fu et al. (2000).

Analytical methodology

The shear stress distribution based on the Fu et al. (2000) model at both interfaces is depicted in Fig. 16(a) for a GFRP rod glued-in glulam with a bonded length of 100 mm. For ease of comparison the shear stress distribution based on the Volkersen model and for a CFRP rod according to the Fu et al. (2000) model are also illustrated. The material properties from Table 6 were adopted in the model. By adopting the Fu et al. (2000) model, the epoxy shear strength of 18 MPa (see Table 2) was attained at the loaded end of the GFRP rod for a low pull-out load of 2000 N (at x=100 mm, see Fig. 16(a)). At the same load the Volkersen model yields 45% lower shear stress at the loaded end. At the same reference displacement higher shear stress values (up to 1.9 times higher) are recorded at the loaded end of a CFRP rod due to the higher elastic modulus. In a displacement-controlled test earlier debonding is expected in glued-in CFRP rods. The localised debonding in glued-in GFRP rods (Fu et al. (2000) model) due to the peak shear stresses results in slip between the adhesive and the rod (Fig. 15(b)). At this stage a bond stress-slip model with a bilinear curve and a constant frictional component τ_{fr} can be assumed (Fig. 15(d)). The application of the descending branch of the bond stress-slip model (localised debonding) at the rod/adhesive interface in the Fu et al. (2000) model results in a steep increase in the wood/adhesive interfacial shear stresses. Extensive debonding at this interface may be considered since the τ_{wa} values close to the bond and timber shear strength. Based on these analytical findings and the fact that the majority of the experimental failure modes lied in the wood/resin interface, a combined methodology is adopted where full composite action and relative slip between the materials are considered in different regions (Fig. 15(c)) .

Two regions are identified along the bonded length. In the region closer to the loaded end, the wood/adhesive interface is considered the main slip surface area where the bond stress-slip model is applied. This is the ‘partial interaction’ region and the adhesive acts mainly in shear (Fig.15(c)). In the region closer to the free end, termed as ‘full interaction’ region, there is full composite action and displacement compatibility between the materials and the Fu et al. (2000) model is adopted (Fig, 15(c)). To find the transition point between the ‘partial’ and ‘full interaction’ region along the bonded length, a Matlab (Mathworks 2019) script was designed and an iterative approach was followed. The bonded length was discretised into smaller elements (L_e=1.0 mm) and an initial slip value s_1 was assumed at the loaded end. The bond stress, $\tau_1=f(sl_1)$, in the first element was derived from the bond- stress slip models at the wood/adhesive interface summarised in Table 7. The bond stress is assumed constant along each element. The bond force, $\tau_{wa}L_{ph}$, developed in the first segment is transferred to timber as tension and the average timber strain of the second element can be derived as $\varepsilon_w=\tau_{wa}L_{ph}/A_w$, where L_{ph} is the hole surface area in the discretised element. Here, the effective tension stiffening area is assumed to be equal to the net timber cross-sectional area of the 'pull-pull' test specimens. However, the tension stiffening area varies along the bonded length and is smaller than A_w (Fig. 11). The tensile force of the rod at the second element is reduced by $\tau_{wa}L_{pr}$, $F_{r2}=F_{r1}-\tau_{wa}L_{pr}$, and the slip value is calculated as $sl_2=sl_1$-(dsl/dx)L_e where ds/dx is derived from Eq. (3). It is assumed that the through thickness shear stresses in the adhesive are constant in the 'partial interaction' region and the adhesive carries no tension. Based on the strain value of the second element and the remaining embedment length, the axial rod and timber stresses, interfacial shear stresses and slip values are analytically calculated according to the Fu et al. (2000) model. This process continues along the bonded length until compatibility conditions in τ_{wa}, ε_w, ε_r and sl values are met between the 'partial' and 'full interaction' region. If the boundary conditions are not met, a new slip value at the loaded end is assumed and the iteration continues.

Parametric study

In the analytical study three bond stress-slip scenarios were investigated in order to understand the debonding failure along the bonded length. Two bilinear bond stress-slip models (model A and B) and a linear-frictional model (model C) were used to reflect any differences in the stiffness of the ascending branch and the post-failure performance (material variability). In the model A the average stiffness values K_{e1}, K_{e2} at the wood/adhesive interface were considered as derived from the experimental data in the G_50 group. The maximum slip values, sl_m, were calculated from the average experimental bond strength in the G_50 group and the relevant stiffness values K_{e1}. In the model B the stiffness K_{e1} was increased by considering two times the standard deviation in order to introduce early debonding (lower sl_m compared to the model A). In the model C a sudden drop in the bond strength (K_{e2}=0) was considered, as experimentally observed in most specimens.

Analytical results

The wood/adhesive bond stress, axial rod and timber strain and slip values along the bonded length are depicted in Fig. 17 and 18 at each load cycle and for a bonded length of 100 mm and 140 mm respectively. The different bond stress-slip scenarios were investigated in the final cycle (P=24000 N and P=33600 N for L_b=100 mm and 140 mm respectively). In the model A the full composite action is developed at x=54 mm and x=94 mm from the free end for the G_100 and G_140 group accordingly. The rod and timber strain values vary linearly within the partial interaction region reflecting the linear bond stress-slip behaviour. The bond stress peaks at the free end (x=0 mm) are small (τ_{wa}=0.1-0.4 MPa for the G_100 group and τ_{wa}=0.1-0.6 MPa for the G_140 group) and there is no indication of bond failure at this region. In the model C the debonding region is extensive (up to 130 mm in the G_140 group). Debonding is initiated at the final load cycle irrespective of the bonded length due to exceedance of the maximum bond strength, τ_m. In most specimens 'noises' of impending failure were perceived during the third and fourth cycle of the experiments for both the G_100 and G_140 groups. These noises are postulated to be the outcome of local debonding near the loaded ('partial interaction' region) and transition to the post-failure stiffness in the bond stress-slip models. In the G_100 group the 'full interaction' region is longer in the model B than in the model A due to the higher bond stiffness in the linear ascending branch. This is also reflected in the slope of the linear part in the strain values (Fig. 17(b) and 18(b)). The analytical and experimental slip values at each load cycle are summarised in Table 8.

The analytical models underestimate the tensile slip values at the loaded end. The model C results in the highest slip values (68-73% of the experimental ones) as a result of the low bond stresses developed, τ_{fr}=3.0 MPa, and the required extensive debonding to meet the boundary conditions between the 'full' and 'partial interaction' region. The final slip values in the 'partial interaction' region were corrected for shear deformation due to the bond shear stresses based on Eq. (11) (Fu et al. 2000) accounting for the average axial strain introduced in the analytical methodology (see Fig. 11).

$$u_w(r_a,x)-u_w(r_2,x)=\frac{1}{G_w}\left[\frac{-(1/2)\left(r_2^2-r_1^2\right)+r_2^2\ln(r_2/r_1)}{\left(r_2^2-r_1^2\right)/r_1}-\frac{-(1/2)\left(r_a^2-r_1^2\right)+r_2^2\ln(r_a/r_1)}{\left(r_2^2-r_1^2\right)/r_1}\right]\tau_{wa}(x) \tag{11}$$

where $u_w(r_a,x)$ is the displacement of timber at a distance r_a representing the average axial timber strain deformation and G_w is the timber shear modulus.

The effect of shear is expected to increase the slip values at the loaded end as shown in Fig. 11. The same rods when tested with the 'pull-compression' method in (Toumpanaki and Ramage 2021) exhibited lower slip values (approximately 50% of the ‘pull-pull’ test method) at the loaded end attributed to the restricting effect of the reaction frame. The analytical tensile slip values in the ‘50-100’ mm region on the timber face are overestimated and this is attributed to the tension stiffening effect. It is expected that the axial timber strains are lower at the outer perimeter of the specimen and the current methodology assumes that the bond force is uniformly distributed over the full timber cross-sectional area. However, when debonding takes place (Model C) at the final load cycle, the analytical slip values are similar to the experimental ones (e.g. Cycle 4 of the G_140 group). The lower timber elastic modulus in the wood crushing region at the loaded end face is expected to affect the slip values and this is not considered here. The tensile slip values in the ‘100-140’ mm region in the G_140 group agree well with the experimental values suggesting full composite action. A decreasing trend in the ‘50-100’ and ‘100-140’ analytical slip values of the model C is observed with increasing load cycle. This is attributed to debonding at the last load cycles, as also observed in the experimental slip measurements. The average timber strain values along the bonded length are lower in the model C compared with the models A and B, as shown in Fig. 17(c) and Fig. 18(c) for the GFRP_100 and GFRP_140 group respectively in cycle 4. Therefore, the timber end face deformation and relevant slip do not increase in the last load cycles in the model C. Debonding (model C) results in lower slip values (higher stiffness) in the ‘50-100’ region. Higher slip values (lower stiffness) due to the composite action are calculated in the 100-140 region in the GFRP_140 group (see Table 8 and Figure 9). The slip values under compression considering composite action agree fairly well with the experimental findings (Table 8). The experimental compressive slip values in the G_140 group at the last two cycles are higher in the ‘50-100’ mm region than in the ‘100-140’ mm region. Debonding in the ‘50-100’ region and deviation from composite action result in higher slip values under compressive load. More experimental data is needed to build confidence in the analytical slip predictions.

Conclusions

The GFRP rods exhibit higher axial load capacity than the CFRP rods and this is attributed to the greater extrusions in the outer surface of the GFRP rods (greater mechanical interlocking effect). However, the CFRP rods exhibit higher axial tensile stiffness at both SLS and ULS as a result of their higher longitudinal elastic modulus. An increase in the axial load capacity of the GFRP rods was recorded with increasing bonded length. This increasing trend tends to plateau at anchorage lengths greater than 10*D* due to the progressive bond degradation. The GIROD design formula developed for the pull out resistance of glued-in steel rods provides a conservative estimate of the axial load resistance of glued-in FRP rods. To reflect the differences in the mechanical interlocking effect and axial withdrawal capacity between FRP rods, a bond factor should be considered in the GIROD design formula. The material viscous damping ratio due to cyclic loading ranged from 4.4-13.0% and decreased with increasing load cycle. Substantial differences exist at the axial stiffness of glued-in rod connections under tension and compression and along the bonded length attributed to the bond stress transfer mechanism, tension stiffening effect, potential construction tolerances and debonding at higher loads. The tensile stiffness values were substantially lower at the loaded end compared with the ones along the bonded length due to the reference slip values, shear lag and tension stiffening effect. The wood crushing effect decreases the compressive stiffness at the loaded end compared with the values recorded along the bonded length. Gaps between structural elements in glued-in rod connections due to either long-term effects or construction tolerances can decrease the compressive stiffness by 67-75% compared with a full contact scenario. This decrease in compressive stiffness should be considered when glued-in FRP splice joints are simulated due to a load reversal scenario. An analytical model is introduced that defines a 'partial' and 'full interaction' region based on the relative slip between the materials and the deviation from a composite action. The analytical tensile slip values agree better with the experimental results when local debonding (low bond frictional component) is adopted in the bond stress-slip models. The compressive slip values assuming composite action agree fairly well with the experimental findings. Deviations due to the assumed tension stiffening area and local wood crushing and debonding under compression should also be considered. GFRP rods exhibit overall equivalent strength and stiffness to CFRP rods and they are promising for timber applications considering their comparatively lower initial cost (~1/3 of CFRP cost). Yet, the long-term mechanical performance of glued-in GFRP rods should also be evaluated.

Data availability statement

All data, models, or code that support the findings of this study are available from the corresponding author upon reasonable request.

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Notation

The following symbols are used in this paper:

A_{eff} =effective tensile area in timber (tension stiffening effect) (mm^2);

A_r =cross-sectional area of the rod based on the core diameter (mm^2);

A_{ro} =cross-sectional area of the rod based on the outer apparent diameter (mm^2);

A_w =net timber cross-sectional area (mm^2);

B_{15} =coefficient in the bond strength equation of the Fu et al. (2000) model;

B_{26} =coefficient in the bond strength equation of the Fu et al. (2000) model;

C_{21} =coefficient in the bond strength equation of the Fu et al. (2000) model;

C_{22} =coefficient in the bond strength equation of the Fu et al. (2000) model;

D =core diameter of the rod (mm);

D_h =hole diameter (mm);

D_o =outer apparent diameter of the rod (mm);

E_e =elastic energy stored during cyclic loading (kNmm);

E_r =longitudinal elastic modulus of the rod;

E_p $=E_{storage}-E_e$ =Energy dissipation (kNmm)

$E_{storage}$ =total energy stored during cyclic loading (kNmm);

E_w =longitudinal elastic modulus of timber (GPa);

e =edge distance of glued-in rods (mm);

F_r =axial load (kN);

F_{ru} =failure load (kN);

$F_{rult,\ mon}$ =ultimate failure load under monotonic tensile loading (kN);

$f_{cw,0,m}$ =timber mean compressive strength (MPa);

f_{ru} =mean tensile strength (MPa);

$f_{tw,0,m}$ =timber mean tensile strength (MPa);

f_v =shear strength (MPa) ;

$f_{vw,//,m}$ =timber mean shear strength parallel to the grain (MPa);

$f_{vw,\perp,m}$ =timber mean shear strength perpendicular to the grain (MPa);

G =bond fracture energy (MPa·mm);

G_a =adhesive shear modulus (GPa);

G_v =shear modulus (MPa);

G_w =timber shear modulus (MPa);

G_{II} =mode II fracture toughness (MPa·mm)

K_{e1} =bond stiffness in the linear ascending branch of the bond stress-slip model (MPa/mm);

K_{e2} =bond stiffness in the linear descending branch of the bond stress-slip model (MPa/mm);

K_{II} =stress intensity factor (MPa·mm$^{1/2}$);

k_b =bar type coefficient in the NZTDS (2007) design equation for the axial load capacity of glued-in rods

k_e =epoxy coefficient in the NZTDS (2007) design equation for the axial load capacity of glued-in rods

k_m =moisture coefficient in the NZTDS (2007) design equation for the axial load capacity of glued-in rods

L_b =bonded length (mm);

L_e =length of a discretised element = 1mm;

L_m =material factor in the GIROD design formula;

L_{ph} =$\pi D_h L_b$ = hole surface area (mm^2);

L_{pr} =πD =rod perimeter (mm)

L_{un} =free unbonded length;

l_{geo} =geometrical factor in the GIROD design formula;

MC =Moisture Content (%);

p_a =constant in the Lamè form of the through thickness shear stress in adhesive (Fu et al. (2000) model);

p_w =constant in the Lamè form of the through thickness shear stress in timber (Fu et al. (2000) model);

q_a =constant in the Lamè form of the through thickness shear stress in adhesive (Fu et al. (2000) model);

q_w =constant in the Lamè form of the through thickness shear stress in timber (Fu et al. (2000) model);

RH =Relative Humidity (%);

r =radial coordinate, radius (mm);

r_o =radial coordinate at the rod/adhesive interface (mm);

r_1 =radial coordinate at the wood/adhesive interface (mm);

r_2 =radial coordinate at the timber face (mm);

u_a =axial displacement in the adhesive (mm);

u_w =axial displacement in timber (mm);

sl =loaded end slip (mm);

sl_{anal} =analytical loaded end slip value (mm);

sl_{exp} =experimental loaded end slip value (mm);

s_m =maximum slip in the ascending branch of the bond stress-slip model (mm);

s_u =maximum slip in the descending branch of the bond stress-slip model (mm);

t_a =glue-line thickness (mm);

x =horizontal coordinate in the horizontal axis along the bonded length (mm);

α_L =thermal coefficient of wood in the longitudinal direction;

α_R =thermal coefficient of wood in the radial direction;

α_T =thermal coefficient of wood in the transverse direction;

β =coefficient in the bond strength equation of the Fu et al. (2000) model;

γ_a =adhesive shear strain;

ε_r = longitudinal rod strain;

ε_{ru} =elongation at break;

ε_w =longitudinal timber strain;

ν =Poisson's ratio;

ξ $= \xi = E_p/2\pi \cdot E_{storage}$ =Damping ratio (%);

ρ_k =characteristic timber density (kg/m^3);

$\rho_{,mean}$ =mean timber density (kg/m^3);

σ_{ro} =rod axial stress (MPa)

τ =bond strength (MPa);

τ_a =through thickness shear stresses in the adhesive (MPa);

τ_{fr} =frictional bond strength (MPa);

τ_m = maximum bond strength in the ascending branch of the bond stress-slip model (MPa);

τ_{ra} =bond strength at the rod/adhesive interface (MPa);

τ_w =through thickness shear stresses in the wood (MPa);

τ_{wa} =bond strength at the wood/adhesive interface (MPa);

ω =parameter in the GIROD design formula for the axial load capacity of glued-in rods;

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List of Figures

Fig. **16.** (a) bond shear stress distribution for ε_r=4.2x10^{-4} and (b) bond shear stress distribution for *P*=12000N.

Fig. 17. GFRP_100 group (a) wood/adhesive bond stresses, (b) rod strain values, (c) timber strain values and (d) slip values.

Fig. 18. GFRP_140 group (a) wood/adhesive bond stresses, (b) rod strain values, (c) timber strain values and (d) slip values.

Table 1. Summary of selective design formulas for the axial load capacity of glued-in rods.

Design Guideline	Equation	Nomenclature
DIN 2010 (DIN 2010)	$\pi D L_b \tau$	where τ=4.0 MPa, $L_b \leq 250$ mm τ=5.25-0.005L_b MPa, 250 mm$\leq L_b \leq$500 mm
GIROD (SP 2002)	$\pi D L_b \tau \dfrac{\tanh\omega}{\omega}$	where $\omega=\sqrt{\frac{l_{geo}}{l_m}}$, $l_{geo}=\frac{\pi D L_b^2}{2}\left(\frac{1}{A_r}+\frac{\frac{E_r}{E_w}}{A_w}\right)$ and $l_m={E_r G}/{\tau^2}$
Riberholt (Riberholt 1988)	$\tau \rho_k D L_b$ $\tau \rho_k D L_b^{0.5}$	where τ=0.037 epoxy strength factor for L_b<200 mm and τ=0.52 epoxy strength factor for $L_b \geq$200 mm
New Zealand (NZTDS 2007)	$6.73 k_b k_e k_m$ $(L_b/D)^{0.86}\,(D/20)^{1.62}\,(D_h/D)^{0.5}\,(e/D)^{0.5}$	where k_b=bar type coefficient=1.0, k_m=moisture coefficient=1 (MC<18%) and k_e=epoxy type coefficient=1.0

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Note: where D= rod diameter, D_h= hole diameter, L_b=anchorage length, E_r=46 GPa and E_w=11.6 GPa are the tensile Young's modulus of the GFRP rod and timber respectively, A_r and A_w is the cross-sectional area of the GFRP rod and timber specimen, ρ_k= characteristic timber density (kg/m^3), e= edge distance and G= the bond fracture energy derived according to (Gustafsson and Serrano 2002)
Note: the material and geometrical properties in GIROD were derived from the relevant experimental data as proposed in (Gustafsson and Serrano 2002), e.g. τ=5.55 MPa and G=1.7 MPa mm in (Zhu et al. 2017) and τ=9.7 MPa and G=4.6 MPa mm in the current study

Table 2. Material properties of FRP rods, epoxy glue and timber.

Properties	CFRP (Carbopree)	GFRP (Glasspree)	Epoxy glue (Sikadur 30)	Timber
Longitudinal tensile elastic modulus, E (GPa)	130[1] / 134.2[7]	46[1]/ 60.6[7]	11.2[1] / 13.8 ± 1.3[2] (5)	N/A
Average tensile strength, f_{ru} (MPa)	2450[1]	1000[1]	26[1] / 24.6 ± 4.8[2] (5)	N/A
Elongation at break, ε_{ru} (%)	1.8[1]	1.8[1]	0.2 ± 0.05[2] (5)	N/A
Poisson's ratio, v	N/A	N/A	0.25 ± 0.03[2] (5)	N/A
Stress intensity factor, K_{II} (MPa·mm$^{1/2}$)	N/A	N/A	77.3 ± 14.8[3] (6)	N/A
Mode II fracture toughness, G_{II} (MPa·mm)	N/A	N/A	0.43[3,4]	N/A
Shear strength, f_v (MPa)	N/A	N/A	18[1]	N/A
Density, $\rho_{,mean}$ (kg/m^3)	N/A	N/A	N/A	430 ± 4.3[5] (5)
Compressive strength, $f_{cw,0,m}$ (MPa)	N/A	N/A	N/A	45.8 ± 2.3[5] (20)
Tensile strength, $f_{tw,0,m}$ (MPa)	N/A	N/A	N/A	72.6 ± 12.3[6] (7)
Shear strength // to grain, $f_{vw,//,m}$ (MPa)	N/A	N/A	N/A	6.4 ± 1.4[5] (10)
Shear strength ⊥ to grain, $f_{vw,\perp,m}$ (MPa)	N/A	N/A	N/A	8.7 ± 1.6[6] (10)

Note 1: [1]Nominal values as provided by the manufacturer, [2] Experimentally measured values according to BS EN ISO 527 (BSI 2019), [3] Experimentally measured values according to Ayatollahi et al. (2011), [4] $G_{II}=K_{II}^2/E$, [5] Experimentally measured values according to BS 373:1957 (BSI 1957), [6] Experimentally

measured values according to ASTM D143 (ASTM 2009), [7] Experimentally measured values from 'pull-compression tests' in Toumpanaki and Ramage (2021) using the core cross-sectional area of the rods.

Note 2: average value ± standard deviation (No of specimens).

Table 3. Experimental Programme – Cyclic loading regime.

Name	Type of specimen	Bonded length (mm)	No of specimens				Cyclic loading regime (% $F_{rult,mon}$)
			Monotonic tensile loading	Cyclic I	Cyclic II	Cyclic III	
C_50_CIII_1						x	20,40,60
C_50_CIII_2						x	20,40,60
C_50_CIII_3	CFRP	50				x	20,40,60
C_50_CIII_4						x	20,40,60
G_50_CIII_1			-	-	-	x	15,30,45
G_50_CIII_2						x	15,30,45
G_50_CIII_3	GFRP	50				x	15,30,45
G_50_CIII_4						x	15,30,45
G_100_M_1			x				-
G_100_CI_1				x			20,40,60,80
G_100_CII_1	GFRP	100			x		20,40,60,80
G_100_CII_2					x		20,40,60,80
G_100_CII_3					x		20,40,60,80
G_140_M_1			x				-
G_140_CI_1				x			20,40,60,80
G_140_CII_1	GFRP	140			x		20,40,60,80
G_140_CII_2					x		20,40,60,80
G_140_CII_3					x	-	20,40,60,80

Note: $F_{rult,mon:}$ Ultimate failure load under monotonic tensile loading.

Table 4. Experimental results.

Specimen	**Failure load (kN)**	**Bond strength, τ_{ra} (MPa)**	**Bond strength, τ_{wa} (MPa)**	**Loaded end slip, *sl* (mm)**	**Secant stiffness at ULS (kN/mm)**	**Secant stiffness at SLS (kN/mm)**	**Failure mode**
C_50_CIII_1	18.2	11.6	7.3	0.7	28.9	38.3	R/A
C_50_CIII_2	21.6	13.8	8.6	0.7	42.4	38.1	R/A
C_50_CIII_3	18.4	11.7	7.3	0.4	46.4	62.5	W/A
C_50_CIII_4	20.7	13.2	8.3	0.6	41.2	51.1	R/A

G_50_CIII_1	26.7	17.0	10.6	1.0	26.5	38.6	WP + W/A + S
G_50_CIII_2	23.2	14.8	9.2	0.7	39.3	58.0	W/A
G_50_CIII_3	27.4	17.4	10.9	1.3	26.9	30.8	WP + W/A
G_50_CIII_4	20.0	12.8	8.0	0.8	32.4	27.0	W/A
G_100_M_1	30.0	9.5	6.0	1.9	15.6	32.8	W/A
G_100_CI_1	37.6	12.0	7.5	1.5	25.4	30.8	W/A
G_100_CII_1	37.7	12.0	7.5	1.6	26.0	35.7	WP + W/A
G_100_CII_2	38.2	12.1	7.6	1.3	29.3	41.8	W/A
G_100_CII_3	40.3	-	-	-	-	-	Anchorage failure
G_140_M_1	41.9	9.5	6.0	1.3	55.0	116.2	W/A + S
G_140_CI_1	41.6	9.5	5.9	1.7	25.7	31.4	WP + W/A
G_140_CII_1	43.6	9.9	6.2	1.7	28.5	33.8	W/A +R/A + S
G_140_CII_2	37.9	8.6	5.4	1.5	27.3	29.1	W/A
G_140_CII_3	43.1	9.8	6.1	1.6	27.6	36.8	W/A

Note: R/A: resin/adhesive rod interface failure, W/A: wood/adhesive interface failure, WP: wood plug failure, S: splitting.

Table 5. Damping ratios.

Group	**Damping ratio, ξ (%)**			
	Cycle 1	**Cycle 2**	**Cycle 3**	**Cycle 4**
C_50	10.9 (2.1)	8.4 (2.3)	5.1 (1.2)	-
G_50	12.3 (4.9)	9.9 (1.8)	7.9 (1.6)	-
G_100	11.4 (1.7)	9.4 (3.7)	6.8 (2.2)	4.4 (1.4)
G_140	13.0 (2.1)	9.2 (2.3)	7.0 (1.8)	7.4 (1.7)

Note: average value (standard deviation).

Table 6. Material properties of the analytical model.

Properties	Timber	GFRP	Epoxy
Longitudinal tensile elastic modulus – E (GPa)	11.6 [1]	60.6 [2]	11.2 [1]
Average tensile strength, f_{ru} (MPa)	16.5 [1]	1000	24.6 [2]
Shear Modulus – G_v (MPa)	700	N/A	4300
Shear strength – f_v (MPa)	8.7 [2]	N/A	18 [1]
Radius, r (mm)	35	5	8

Note: [1] values for GL24h, [2] Experimental values.

Table 7. Bond stress-slip scenarios of the analytical study.

bond stress-slip model	wood/resin interface
Model A	$\tau_{wa}=K_{e1}sl$, $sl\leq sl_m$ $\tau_{wa}=K_{e2}(sl_u-sl)$, $sl_m\leq sl\leq sl_u$ $\tau_{wa}=\tau_{fr}=3.0$ MPa, $sl\geq sl_u$ where $K_{e1}=15.4$ MPa/mm, $K_{e2}=14.8$ MPa/mm, $sl_m=0.6$ mm and $sl_u=1.3$ mm
Model B	$\tau_{wa}=K_{e1}sl$, $sl\leq sl_m$ $\tau_{wa}=K_{e2}(sl_u-sl)$, $sl_m\leq sl\leq sl_u$ $\tau_{wa}=\tau_{fr}=3.0$ MPa, $sl\geq sl_u$ where $K_{e1}=26.4$ MPa/mm, $K_{e2}=14.8$ MPa/mm, $sl_m=0.4$ mm and $sl_u=1.0$ mm
Model C	$\tau_{wa}=K_{e1}sl$, $sl\leq sl_m$ $\tau_{wa}=\tau_{fr}=3.0$ MPa, $sl\geq sl_m$ where $K_{e1}=26.4$ MPa/mm and $sl_m=0.4$ mm

Table 8. Experimental and analytical slip values.

Load	Region (mm)	Slip (mm)	G_100				G_140			
			Cycle 1	Cycle 2	Cycle 3	Cycle 4	Cycle 1	Cycle 2	Cycle 3	Cycle 4
Tension	0 (LE)	sl_{exp}	0.36	0.56	0.67	0.89	0.70	1.00	1.32	1.74
		sl_{anal}	0.14	0.28	0.43	0.57 (A) 0.54 (B) 0.65 (C)	0.20	0.40	0.60	0.70 (A) 0.82 (B) 1.19 (C)
	50-100	sl_{exp}	0.002	0.003	0.004	0.003	0.003	0.004	0.006	0.007
		sl_{anal}	0.004	0.008	0.012	0.016 (A) 0.016 (B) 0.009 (C)	0.006	0.011	0.017	0.022 (A) 0.017 (B) 0.007 (C)
	100-140	sl_{exp}	-	-	-	-	0.006	0.011	0.014	0.015
		sl_{anal}	-	-	-	-	0.005	0.009	0.013	0.018 (A) 0.018 (B) 0.011 (C)
Compression	50-100	sl_{exp}	0.004	0.010	0.015	0.019	0.010	0.018	0.031	0.042
	100-140	sl_{exp}	-	-	-	-	0.008	0.015	0.023	0.031
		sl_{anal}	0.005	0.010	0.015	0.020	0.006	0.011	0.017	0.023

Note: sl_{exp}=average experimental slip, sl_{anal}=analytical slip and LE=loaded end.