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EFFECT OF ELEVATED TEMPERATUREs ON THE MECHANICAL PERFORMANCE OF PULTRUDED FRP JOINTS WITH A SINGLE ORDINARY OR BLIND BOLT

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ABSTRACT

Presented in this paper is a combined experimental and analytical modelling study of the strength of pultruded FRP single bolted double-lap joints subjected to tensile loading and elevated temperatures. Dynamic mechanical analysis (DMA) and thermogravimetric analysis (TGA) are conducted on the polymeric composite material to determine the glass transition temperature and decomposition temperature, respectively. Based on the DMA and TGA results, and to cover glass transition without any material decomposition, the six temperatures selected for the test program are +23 ^{o}C, +60 ^{o}C, +100 ^{o}C, +140 ^{o}C, +180 ^{o}C and +220 ^{o}C. Three nominally identical joints are tensioned to failure at each temperature. A total of 36 double-lap joints are tested, comprising 18 joints fabricated with ordinary steel bolting and the other 18 with novel blind bolting. A comparison is made based on load-displacement curves, failure modes and maximum (ultimate) loads. It is found that both methods of mechanical fastening experience a reduction of 85% in maximum load as the test temperature increases from +23 ^{o}C to +220 ^{o}C. Three proposed empirical or mechanism-based models for characterising strength under elevated temperatures are shown to provide good predictions for the maximum loads obtained in the test program.

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INTRODUCTION

Pultruded fibre reinforced polymer (PFRP) composites are thin-walled shapes that have constant cross-section along their straight length. Over the last 20 years they have witnessed increasing R&D (Mottram, 2015), and have been adopted in new all-FRP constructions (Bank, 2006; Pendhari *et al*., 2008). Their attraction in civil engineering is mainly due to their advantages in reduced manufacturing cost, light weight, ease of installation, and low maintenance cost because of their resistance to harsh environmental conditions (Hollaway, 1993; Bakis *et al*. 2002).

Connecting PFRP shapes in structural systems is the key to providing structures that are reliable and possess structural integrity (Bank, 2006; Mottram and Turvey, 2003). For connecting elements, steel bolting is a preferred connection method because of its low cost, ease of installation/disassembly and straightforward inspection procedures with manageable quality control (Turvey, 2000; Mottram and Turvey 2003). In physical situations where access for tightening the bolting from both sides is restricted (such as when connecting tubular hollow shapes (Wu *et al*., 2014)), blind bolts, requiring access from one side only offer a convenient engineering solution (Evernden and Mottram, 2006).

It is well known (Wong and Wang, 2007; Wang *et al*., 2011; Bai *et al*., 2008; Correia *et al*. 2013) that the mechanical properties of PFRP materials degrade when the temperature reaches and exceeds the glass transition temperature (T_g). What is not well understood is the effect of elevated temperature on the mechanical response of bolted joints loaded to ultimate failure.

Although bolted joints in PFRP structures are subject to complex stress states (Turvey 2000; Bank 2006), it has been shown in Girão Coelho and Mottram (2015) that to understand bolted joint response and failure we first are to characterize PFRP joints under a single stress condition. As an example of this strategy, Kim and Whitney (1976) investigated the pin-bearing strength of laminated composites under hot-wet conditions. Three graphite/epoxy laminates were kept in a humidity chamber at a relative humidity of 98% until the specimens showed a weight increase of 1.5%. Then the specimens for a single steel bolted joint were tested at a moderate temperature of 126.7 ^{o}C (260 ^{o}F). The results showed a maximum strength reduction of 40% after the aging conditioning. In a study by Scarponi *et al*. (1997), single steel bolt joints of T300/934 carbon fibre/epoxy laminate were tested under combined changes in temperature and lateral tightening torque. The test matrix included five temperatures in the range -150 ^{o}C to +80 ^{o}C, with four tightening torques to the steel bolting of 0 Nm, 5 Nm, 30 Nm and 50 Nm. It was found that the bearing strength reduced from 356 MPa to 313 MPa when the temperature increased from room temperature to +80 ^{o}C without a bolt tightening torque. Tightening the single 9.4 mm diameter bolt to 50 Nm significantly increased the bearing strength by over four times from 313 MPa to 1371 MPa, even when the temperature was +80 ^{o}C. Hirano *et al*. (2007) studied the effects of temperature on the pin-bearing strength of two carbon FRPs. In their test matrix the three temperatures adopted were -100 ^{o}C, +25 ^{o}C and +150 ^{o}C. The change in failure mode was recorded over the temperature range and strength decreased by a maximum of 41%. Although these previous studies have contributed knowledge to the understanding of the thermal-mechanical response of FRP bolted joints, they used aerospace carbon FRPs that respond to bolt bearing load differently to PFRPs.

Turvey and Wang (2001, 2007a, b, 2009a, b), and Zafari and Mottram (2012) performed series of tests with bolted connections that showed that there were strength reductions when the material was PFRP. Single bolted joints (10 mm diameter hole and 9.8 mm diameter bolt (*D*)) were tested by Turvey and Wang (2007b) in batches of three under tension load at room temperature, +60 ^{o}C and +80 ^{o}C. Bolt tightening was to the ‘finger tight’ condition (defined as the tightness attained by the resistance to bolt tightening using human fingers only; it will provide through-thickness lateral restraint once the bolted joint is loaded). Two geometrical configurations for the double-lap joints were arbitrarily found to achieve bearing failure or net tension failure at room temperature, respectively. All bearing-designed joints showed the same consistent bearing failure mode at the elevated temperatures. A reduction of 39% in bearing strength (there was lateral restraint with the steel bolting) was found at +60 ^{o}C and a higher decrease of 51% was found at +80 ^{o}C. More significant reductions were determined with the net tension designed joints, with 49% reduction at +60 ^{o}C and 56% reduction at the highest temperature of +80 ^{o}C. This higher reduction for net tension was associated with a change of the failure mode from tension (at room temperature) to bearing (at +60 ^{o}C and +80 ^{o}C).

Turvey and Wang (2009a) tested PFRP joints having two bolts in a single column at room temperature and +60 ^{o}C. The geometric configurations studied included three end (edge) distance-to-bolt diameter ratios (E/D), two pitch distance-to-bolt diameter ratios (P/D) and two side distance-to-bolt diameter ratios (S/D). A joint’s ultimate load was defined as the maximum load that it resisted, whereas the damage load was when there was first evidence of a reduction for a change in joint stiffness in the (linear) load-displacement response. It was found that changing the three geometric ratios had an effect on the thermal-mechanical properties (damage load and ultimate load). Test results showed that when the temperature was increased to +60 ^{o}C, the average reduction in ultimate load was 17%, and for damage initiation it was higher at 42%, regardless of the geometric configuration. It is interesting to note that, when E/D was 4, P/D was 2 and S/D was 4, the maximum strength reductions were recorded for ultimate load at 36% and for damage load at 59%. Zafari and Mottram (2012) reported a study for the pin-bearing strength of an PFRP material for the web of a wide flange shape. Specimens were soaked in water for 3000 hours at +40^{o}C before pin-bearing loading at room temperature. The test matrix involved the presence of a clearance hole and four steel pins (plain bolt shafts) for diameters of 10 to 25 mm. It was found that when the bearing load was in the pultrusion direction, the average strength reduction was 30% for increasing shaft diameter.

Previous studies with PFRP materials do provide some insight for understanding temperature effects on the mechanical behaviour of bolted joints. One limitation in their scope of application is that the temperature has ranged up to +80 ^{o}C, and this only covers the initial stage of the glass transition process. Work is required to understand mechanical performance when elevated temperatures encompass the full range of glass transition and toward the decomposition temperature. FRP structures may experience temperatures higher than +80 ^{o}C in extreme events such as localized heating from a fire. To have the data to design for safety, characterisation of the mechanical response of PFRP joints is essential over a higher temperature range, including T_{g}.

Because blind bolts are convenient when access for ordinary bolting is poor, a novel type of blind bolt has been included in the test program. Wu *et al*. (2014) reported on both static and fatigue results for PFRP double-lap joints with this blind bolting, but not for temperatures higher than room temperature. This paper presents new test results for joints with a single bolt to an elevated temperature of +220 ^{o}C. To establish the temperatures in the test program, dynamic mechanical analysis (DMA) and thermogravimetric analysis (TGA) tests were conducted with the PFRP material to determine T_{g} and decomposition temperature (T_{d}). On the basis of these measurements, the experimental temperatures were selected to be +23 ^{o}C, +60 ^{o}C, +100 ^{o}C, +140 ^{o}C, +180 ^{o}C and +220 ^{o}C, which exceeded T_g without the occurrence of material decomposition. A total of 36 PFRP joints were failed and their load-displacement curves were constructed. Damage and ultimate loads from the 18 ordinary and

18 blind bolted joints were compared. Three existing models for strength change with temperature were assessed and compared in terms of their reliability and relevance to predict the maximum loads for a single bolted joint at elevated temperatures.

EXPERIMENTAL PROGRAM

Materials

The PFRP plate with a thickness of 5.5 mm was supplied by Nanjing Xingya FRP Co. Ltd. The same polymeric composite material was used in the bolted joint study by Wu *et al*. (2014). It consists of E-glass fibre reinforcement embedded in a polyester resin matrix. Fibre volume fraction and fibre architecture were characterised according to ASTM D-3171, and full details are reported in Wu *et al*. (2014). The overall fibre volume fraction is 48%. The plate lay-up has a symmetric and balanced reinforcement scheme, with rovings in the core, sandwiched between two layers of a continuous strand mat (CSM). Measured tensile properties in directions longitudinal and transverse to the pultrusion direction are reported by Wu *et al*. (2014), following tensile coupon tests in accordance with ASTM D 3039. The longitudinal tensile modulus and strength are 32 GPa and 393 MPa, respectively, and in the transverse direction these properties are lower at 5 GPa and 22 MPa, respectively. Using the 10^{o} off-axis tensile test method, detailed by Chamis and Sinclair (1976), the mean in-plane shear strength is 25.4 MPa from testing a batch of ten coupons.

The ordinary bolts, 45 mm in length, are made of M10 zinc-plated steel and supplied by Exafast. The steel grade is 4.6, with a nominal tensile strength of 400 MPa, which is equivalent to a Grade A bolt as specified in ASTM A307. The ordinary bolt has a tensile capacity of 13.9 kN and a single shear capacity of 9.3 kN, according to BS 5950. The measured diameter of the shank (smooth part) is 9.8 mm. The blind bolts, 60 mm in length, are M10 high tensile yellow-zinc plated, and were supplied by Blind Bolt Australia. The tensile capacity of the blind bolt is 12.9 kN and the single shear capacity over the thread is

23.2 kN. The measured shank diameter is 9.93 mm. The washers for both bolt types are zinc-plated fenders with inner diameter 10 mm, outer diameter 25.4 mm and thickness 1.75 mm. For ordinary bolted joints, two washers were placed beneath bolt head and nut. For blind bolting a single washer was used on the accessible side of the bolt. For the installation process of a blind bolt the reader is invited to consult the detailed description given by Wu *et al*. (2014).

DMA Testing

DMA was performed with the PFRP plate material to obtain the temperature-dependent mechanical properties of storage and loss moduli. These test results enabled determination of the glass transition temperature (T_g) required to know the elevated temperatures for the test program. A Q800 dynamic mechanical analyser from TA Instruments was used in accordance with ASTM D5023-07. Rectangular specimens of PFRP were cut with dimensions 60 mm by 12 mm, with the longer sides parallel to the direction of pultrusion. A specimen was tested in a three-point bending set-up at a dynamic oscillation frequency of 1 HZ. Scanning was carried out over a temperature range from -40 ^{o}C to +300 ^{o}C, at four different heating rates of 3 ^{o}C/min, 5 ^{o}C/min, 7.5 ^{o}C/min and 10 ^{o}C/min.

As typical examples of DMA results, the three curves plotted in Fig. 1 are for the storage modulus, E' (solid line), loss modulus, E'' (dashed line), and damping factor, given as tan δ (long-short dashed line), at a heating rate of 10 ^{o}C/min. E' represents the elastic modulus of the 60×12×5.4 mm specimen in flexure. T_g in this paper is defined as the temperature at which the peak of the E'' is reached. In the second row of columns (2) to (5) in Table 1 are reported the T_gs for the four heating rates. It is seen from the DMA characterization that T_g increased by 10 ^{o}C from 143 ^{o}C to 153 ^{o}C as the applied heating rate increased from 3 ^{o}C/min to 10 ^{o}C/min.

TGA Testing

TGA was performed in order to determine the decomposition temperature, T_d. Testing was carried out using an STA 409 PC/PG simultaneous thermogravimetry and differential scanning calorimetry analyser from NETZSCH. Specimens were created by grinding the PFRP material into a powder using a rasp. Samples were taken throughout the plate's 5.5 mm thickness to ensure that the fibre and resin content in the powder samples was representative. The analyser took scans from room temperature up to 800 ^{o}C with the sample in a nitrogen atmosphere, having a flow rate of 10 ml/min. As with the DMA testing, the four heating rates were 3 ^{o}C/min, 5 ^{o}C/min, 7.5 ^{o}C/min and 10 ^{o}C/min.

Plotted as a solid curve in Fig. 2 is the remaining mass of the sample versus the increase in temperature at the heating rate of 10 ^{o}C/min. The mass reduction rate curve, shown as a dashed curve in the figure, was constructed from the derivation of the remaining mass curve. According to Kale *et al.* (2006), T_d is determined when the maximum mass reduction rate is achieved. For the four heating rates, the third and fourth rows in Table 1 report T_d and the corresponding remaining mass as a percentage. The PFRP plate is found to decompose at a temperature in excess of 365 ^{o}C, and at 800 ^{o}C the remaining mass is 77.4% (mainly the fibres and matrix additives). In the resin burn-off test procedure by Ye *et al.* (1995) which is used to establish volume fractions of the constituents the required constant furnace temperature (for 2 hours) is under 600 ^{o}C.

PFRP Bolted Joints for Tensile Testing under Elevated Temperatures

Fig. 3 presents the details and dimensions of the double-lap single bolted joints. Fig. 3a shows side and plan engineering drawings; photographs for the same views are given in Fig. 3b. All joints had the same total length of 306 mm and width of 80 mm. Dimensions chosen for the PFRP joint detailing (whiter plates illustrated in Fig. 3) were specified using the EUROCOMP Design Code (Clarke, 1996), and were found also to satisfy the Italian guidance given in CNR-DT 205/2007 (Anonymous, 2008) for the design of PFRP elements.

For the single bolted joint, D is 9.8 mm and the geometrical ratios are E/D of 4.0 and S/D of 4.0, for the width of 8D. The centrally placed hole has diameter 10.5 mm for a clearance hole of about 0.7 mm. As encountered previously by Bai and Keller (2009), premature failure of the outer CSM layers occurred when the PFRP plate was directly clamped by the testing machine grips, especially when the polymeric composite was subjected to tensile loading under elevated temperatures. To avoid this undesirable failure mode, a steel gripping fixture was added at both ends of the PFRP joint, as can be seen by the darker components in Fig. 3. The whiter region of the specimen in Fig. 3 is the bolted joint assembled from three 5.5 mm thick PFRP plates. With the steel fixtures in place the tensile loading could be reliably transferred into the PFRP bolted joint. The steel plates of the same thickness were connected to the PFRP joint by two 8 mm diameter bolts of steel (M8) grade 4.6 in a single row. The measured diameter of the M8 bolting was 7.9 mm. The capacity of the connection between steel and PFRP was designed to be stronger than that of a single 10 mm PFRP bolted connection. It should be noted that, for all specimens, only the plain (smooth) shank of the M10 bolt was in contact with the hole of the inner PFRP plate of the joint.

A clamping force of 3 Nm was applied to the single M10 bolt using a calibrated torque wrench. This relatively low tightening torque was chosen to negate the complication of a significant long-term reduction in clamping tension from material creep and viscoelastic relaxation (Cooper and Turvey, 1995; Mottram, 2005). To facilitate a fair comparison, the 18 joints with the blind bolting method of connection had the same dimensions and bolt torque as did the 18 ordinary bolted joints.

Experimental Set-up and Test Method

The experimental set-up is shown in Fig. 4. All tensile tests were carried out using an Instron 5982 Dual Column Testing System machine with a load capacity of 100 kN. For the temperature control at elevated temperatures the testing machine has an Instron 3119-408

Environmental Chamber with a maximum working temperature of +600 °C.

To monitor temperature, a K-type thermal couple with the sensitivity of ±1 ^{o}C was inserted between the outer and inner PFRP plates of the dummy specimen seen in Fig. 4. The tip of the thermal couple was located close to the bolt region under investigation. The dummy specimen had identical detailing, and its constant temperature was considered to be the same as that of the tested joint specimen. Fig. 4 shows reflective sticker markers attached to the outer and inner PFRP plates. Their vertical separation of 70 mm set an initial gauge length for measurement of the joint's displacement (extension) using an MTS LX 500 non-contact Laser Extensometer with a strain resolution of 1 μm; the scan rate was 100 scans/s. The gauge length was the same in all 36 joints. The extension measured was for the relative displacement of the outer and inner PFRP plates within the joint region; vertical deformation in other regions was excluded.

After installation of the unloaded specimen, the door of the environmental chamber was locked and the temperature inside was increased to the target temperature at a rate of 5 ^{o}C/min. The temperatures of the chamber and the specimen were continuously monitored. When the target temperature was reached, it was kept constant for 30 minutes to ensure that the temperature within the whole specimen was uniform and stable. The soaking time selected was 10 minutes above the minimum recommendation of 20 minutes by Turvey and Wang (2007a). The joint specimen was next loaded to ultimate failure under stroke control at a constant rate of 0.5 mm/min. Load was applied in the pultrusion direction because this is the main load carrying direction in PFRP structures (Bank 2006). Because the joint ultimate failure occurred in a relatively short time (5 minutes to maximum tension) from the onset of stroke controlled loading, the current study did not monitor any creep response.

From the DMA and TGA test results in Table 1, T_g ranged from 149 ^{o}C to 153 ^{o}C and T_d from 368 ^{o}C to 399 ^{o}C. Based on these findings the six test temperatures were +23 ^{o}C,

+60 ^{o}C, +100 ^{o}C, +140 ^{o}C, +180 ^{o}C and +220 ^{o}C, to cover T_g, and to ensure that no PFRP decomposition occurred. For each temperature, three identical joints, for each of the two bolting methods, were tested to obtain a measure of batch variability. The test program thus comprised 18 joints with ordinary bolting and 18 with blind bolting.

The aim of this paper is to obtain the thermal-mechanical degradation of a PFRP bolted joint against elevated temperatures. The influence of loading once the joint was at a target temperature is expected to be higher than if the test method followed a thermal-loading procedure, such as for the fire curve in ASTM E119.

EXPERIMENTAL RESULTS

Table 2 reports the test results for the 36 specimens. In column (1) a label for the specimen is given. In the labelling scheme 'O' is for ordinary bolting and 'B' is for blind bolting. The number following the bolt type represents the target test temperature (+60 ^{o}C, +100 ^{o}C, +140 ^{o}C, +180 ^{o}C or +220 ^{o}C); 'R' in the specimen label stands for room temperature, that is, +23 ^{o}C. The number 1, 2 or 3 after the hyphen in the label is for the order of specimen testing in the batch of three.

The typical failure modes observed under elevated temperatures are presented in Figs. 5a to 5f for ordinary bolting and in Figs. 6a to 6f for blind bolting. Because all joints failed within the inner PFRP (refer to Fig. 3), an outer PFRP plate on one side had to be removed to expose the failure pattern for the photograph. It can be seen in the 12 photographs that shear-out was the final failure mode, regardless of the value of elevated temperature and the type of bolt. Inspection of the images shows that the surface colour of the PFRP material gradually changed from 'white' to 'brown' with the increase of temperature from RT to +220 ^{o}C. A similar colour change was observed for PFRP reinforcing bars under extreme temperatures by Robert and Benmokrane (2010). The work of Asmussen (1983), Burton

(1993), Peutzfeldt and Asmussen (1990) and Tsotsis (1995) indicates that this colour change is likely due to oxidation of the polymer matrix in an air atmosphere.

Plotted in Figs. 7 and 8 are typical load-displacement curves for O and B joints, respectively. The displacement is the separation of the vertical markers as measured by the non-contact laser extensometer. The six temperatures are each represented by a single specimen, with the curves coloured as follows: black for +23 C; red for +60 ^{o}C; green for +100 ^{o}C; blue for +140 ^{o}C; pink for +180 ^{o}C; purple for +220 ^{o}C. Inspection of their characteristics shows an obvious enormous reduction in joint stiffness and maximum load as temperature increased. For joints with ordinary bolting at +23 ^{o}C and +60 ^{o}C the tensile load increased linearly to maximum load for ultimate failure. The load then fell away rapidly as the shear-out mode allowed damage to progress and the axial displacement to reach 20 mm. The lower load level of 2 to 3 kN that was maintained at displacements > 4 mm is mainly attributed to a resistance from the frictional forces created by the lateral restraint of the 3 Nm bolt torque (and difference in thermal expansion). A second contribution (Abd-El-Naby and Hollaway, 1993) to this residual load can be an interlocking mechanism from the shear-out failure having through-thickness deformations. When the temperature exceeded +60 ^{o}C the load-displacement curve is seen to be non-linear prior to the maximum load. In addition, the load reduction after the peak was less rapid when the temperature exceeded +180 ^{o}C. As an example, the purple load-displacement curve in Fig. 7 for +220 ^{o}C temperature is seen to become almost horizontal after the maximum tension.

Fig. 8 indicates that the equivalent load-displacement characteristics of the blind bolted joints are generally similar to those determined with the ordinary bolted joints. At +23 ^{o}C and +60 ^{o}C, the load dropped suddenly after an initial linear increase to the maximum load. Then a residual load, at a similar level to that in the O joints, was obtained once the axial displacement reached 16 mm. A non-linear response before the maximum load is evident when the test temperature is +100 ^{o}C. When the temperature increased above +140 ^{o}C there was no sudden loss in stiffness, and once the maximum load was attained the load-displacement curve continued virtually horizontally at this residual load level.

In Table 2, columns (2) and (3) list the target test temperature (T_{target}) and the measured temperature ($T_{measured}$) when the specimen was loaded (at 0.5 mm/min) to failure. The maximum (peak) load (P_{max}) recorded is reported in column (4). These maximum or ultimate loads were extracted from the corresponding load-displacement curves. Columns (5) and (7) in Table 2 show the average measured temperature ($T_{avg.measured}$), average maximum load ($P_{avg.max}$) from a batch of three specimens (e.g. OR-1 to OR-3), and the percentage reduction in maximum load based on the average (column (6)) at RT. It can be seen from either the P_{max} or $P_{avg.max}$ results in Table 2 that both O and B joints experienced an obvious degradation with the increase of temperature. For the ordinary bolted joints, it is seen that $P_{avg.max}$ changes from 15.5 kN at room temperature to 13.3 kN at +60 ^{o}C, giving a 14% reduction. At +100 ^{o}C the resistance is reduced by 38%, with $P_{avg.max}$ = 9.65 kN. The next 40 ^{o}C increase has a significant effect of lowering strength, as $P_{avg.max}$ is 5.60 kN for a 74% reduction. Further reductions in $P_{avg.max}$ at +180 ^{o}C to 78% and at +220 ^{o}C to 85% have been obtained with the O joints.

A similar reduction trend in $P_{avg.max}$ is observed for B joints at the six test temperatures. $P_{avg.max}$ at +60 ^{o}C is 11.9 kN, which is found to be only 3% below the RT average of 12.3 kN. A significant reduction of 37% occurs when the temperature increases to +100 ^{o}C. At +140 ^{o}C $P_{avg.max}$ is 3.2 kN, only a quarter of its RT value. Further reductions at +180 ^{o}C to 79% and at +220 ^{o}C to 85% occur with the B joints, and it is noteworthy that these are precisely the same percentages as achieved with the ordinary joints. This finding indicates that joint strength at the highest temperature is independent of bolt type.

DISCUSSION OF EXPERIMENTAL RESULTS

Although both O and B joints showed the same shear-out failure mode in Fig. 5 and Fig. 6, respectively, they have different maximum loads. It is evident from the results in Table 2 that $P_{avg.max}$ for the six B batches are lower, on average, by 23% than their six equivalent O batches. From the determination of the change in maximum load using $((P_{avg.max.O} - P_{avg.max.B})/P_{avg.max.O}) \times 100\%$ it can be seen that the relative difference is independent of temperature. For example, at +23 ^{o}C, $P_{avg.max.B}$ is 21% lower than $P_{avg.max.O}$. At the three elevated temperatures of +100 ^{o}C, +180 ^{o}C and +220 ^{o}C, the relative differences are found to be 20%, 24% and 19%, respectively. An overall relationship cannot be established because at +60 ^{o}C the magnitude of $P_{avg.max.B}$ is 11% lower and, with a difference of 42%, there is a second outlier at +140 ^{o}C.

To explain the strength differences between O and B joints, all 36 specimens were disassembled to investigate the detailed interaction between the single blind or single ordinary bolt with its bolt hole. It was found that the contact area between the blind bolt shaft and PFRP plate was reduced due to the slot in the bolt's shaft. This reduction in the contact area is illustrated in Fig. 9 (after testing at +220 ^{o}C). The B bolt is found to have roving reinforcement from the inner PFRP plate packed into the slot opening in the plain length of the steel shaft. The reduced contact area resulting from the detailing of the blind bolt causes a stress concentration state at the bolt hole, leading to failure at a lower strength of the B joint. Similar observations and comments on PFRP joints with blind bolts have been reported by Wu *et al.* (2014).

According to the experimental results in Table 2 both O and B joints experienced a considerable reduction in strength of up to 85% when the temperature reached +220 ^{o}C. This reduction is associated with the matrix dominant failure mode of shear-out. For fire engineering this reduction could be relevant for ultimate limit state design. Note that with the pultrusion composite process it could be impractical to avoid having a matrix dominant failure in bolted joints because of the mechanical properties obtained from having the standard fibre architecture and reinforcement types.

MODELLING OF ULTIMATE LOADS UNDER ELEVATED TEMPERATURES

As seen from the 12 photographs in Figs. 5 and 6 the single bolted joints ultimately failed by the shear-out mode in the inner plate. The strength for this mode of failure can be predicted from using formula (Bank, 2006):

$$P_{\mathrm{sh}} = 2 \times t \times E \times \tau_{\mathrm{LT,T}}. \qquad (1)$$

In Eq. (1), t is 5.5 mm for the thickness of the PFRP plate; E is 40 mm for the distance from bolt centre to the free end of joint, as shown in Fig. 3; $\tau_{\mathrm{LT,T}}$ is the in-plane shear strength of the PFRP plate material as a function of temperature T. Because two sheared surfaces are created for the failure mode, P_{sh} is taken to be the shear force resistance of one surface ($t \times E \times \tau_{\mathrm{LT,T}}$) multiplied by 2. It should be noted that Eq. (1) is based on a number of assumptions, one of which is that forces generated by bolt clamping and by interaction between inner and outer plates can be neglected. A second assumption is that the in-plane shear strength is constant along the shear failure surfaces. Thirdly, it is assumed that the temperature increase in the steel bolt does not have an effect on joint strength. To support this assumption we observe that no yielding or damage in the steel bolting was observed. At 220 $^{\circ}$C the modulus of elasticity of the steel will not have reduced by 10%, and so compared to the PFRP's reduction the bolting appears rigid.

The unknown parameter in the strength equation is $\tau_{\mathrm{LT,T}}$ of the PFRP material at elevated temperatures. Several analytical models for closed-form expressions are proposed in the literature for strength characterisation of FRPs under elevated temperatures. They are either empirical equations based on curve fitting to experimental test results (Mahieux *et al.*

2001; Gibson *et al*. 2006; Feih *et al*. 2007; Correia *et al*. 2013) or based on kinetic description of glass transition (Bai and Keller, 2009). Generally, it is found that the empirical models yield close agreement with experimental results, probably implicit in applying the curve fitting approach. Their weakness is that they lack a physical background, and must rely on the availability of experimental data that is known to be relevant and reliable. On the other hand, any mechanism-based model will require additional data from the physical description of the glass transition stage (as the material state changes from glassy state to leathery state with the breakdown of secondary bonds), and it can be challenging to precisely characterise the required modelling parameters.

In this paper, two empirical models and one mechanism-based model are selected and compared for characterisation of the temperature-dependent in-plane shear strength in Eq. (1).

The empirical model of Feih *et al*. (2007) expresses strength as a function of temperature by:

$$\tau_{\mathrm{LT,T}} = [\frac{\tau_{\mathrm{LT,G}}+\tau_{\mathrm{LT,L}}}{2} - \frac{\tau_{\mathrm{LT,G}}-\tau_{\mathrm{LT,L}}}{2}\tanh\big(\varphi(T-T_k)\big)]R_{\mathrm{rc}}(T)^n \quad (2)$$

where $\tau_{\mathrm{LT,G}}$ and $\tau_{\mathrm{LT,L}}$ are the in-plane shear strengths in a glassy state (a state at room temperature) and in a leathery state (a state after glass transition and before decomposition), respectively; φ and T_{k} are parameters obtained by curve fitting of experimental data; $\mathrm{R}_{\mathrm{rc}}(T)^n$ is a scaling function considering the mass loss during the decomposition process. Because there is no FRP decomposition in the current bolted joint study this parameter is set to1.0.

For the second empirical model, a descriptive model proposed by Correia *et al*. (2013) is based on Gompertz’s distribution. It has the expression:

$$\tau_{\mathrm{LT,T}} = \left(1 - e^{Be^{CT}}\right) \times \left(\tau_{\mathrm{LT},G} - \tau_{\mathrm{LT},L}\right) + \tau_{\mathrm{LT},L} \quad (3)$$

where $\tau_{\mathrm{LT,G}}$ and $\tau_{\mathrm{LT,L}}$ are as in Eq. (2); coefficients B and C are shape and scale parameters determined from fitting the expression to experimental data. Correia *et al*. (2013) showed that the model described by Eq. (3) gave a close prediction for the in-plane shear strength (measured using the 10^o off-axis test method) of a PFRP material.

Bai and Keller (2008) proposed a model based on the well-known rule of mixtures as:

$$\tau_{\mathrm{LT,T}} = \tau_{\mathrm{LT,G}}(1-\alpha_{\mathrm{G,T}}) + \tau_{\mathrm{LT,L}}\alpha_{\mathrm{G,T}}(1-\alpha_{\mathrm{D,T}}) + \tau_{\mathrm{LT,D}}\alpha_{\mathrm{G,T}}\alpha_{D,T}\ . \tag{4}$$

where $\tau_{\mathrm{LT,G}}$ and $\tau_{\mathrm{LT,L}}$ are the same shear strengths as in Eqs. (2) and (3). $\tau_{\mathrm{LT,D}}$ is the shear strength at the decomposition state and $\alpha_{\mathrm{G,T}}$ and $\alpha_{\mathrm{D,T}}$ are the conversion degrees for the glass transition and decomposition at temperature T, respectively. Since there was no PFRP decomposition at +220 ^{o}C it is appropriate to let $\alpha_{\mathrm{D,T}} = 0$. Parameter $\alpha_{\mathrm{G,T}}$ can be characterised based on the kinetic theory via the Arrhenius equation, which is based on Maxwell–Boltzmann distribution:

$$\frac{d\alpha_{\mathrm{G,T}}}{dT} = \frac{A_{\mathrm{G}}}{\beta}\exp\left(\frac{-E_{\mathrm{A,G}}}{RT}\right)(1-\alpha_{\mathrm{G,T}})^{n_{\mathrm{G}}}\ . \tag{5}$$

where A_{G} is the pre-exponential factor, $E_{\mathrm{A,d}}$ is the activation energy and n_{G} is the reaction order. R is 8.314 J/mol.K for the universal gas constant and β is the constant heating rate at 3 ^{o}C/min. Bai and Keller (2008) explain in detailed how to establish values for the model's parameters of A_{G}, $E_{\mathrm{A,d}}$ and n_{G}.

In addition to the rule of mixtures model giving $\tau_{\mathrm{LT,T}}$ in Eq. (4), Bai and Keller (2008) proposed the alternative of using an inverse rule of mixtures approach, which gives the lower bound estimation for $\tau_{\mathrm{LT,T}}$, via:

$$\frac{1}{\tau_{\mathrm{LT,T}}} = \frac{1-\alpha_{\mathrm{G,T}}}{\tau_{\mathrm{LT,G}}} + \frac{\alpha_{\mathrm{G,T}}}{\tau_{\mathrm{LT,L}}} \tag{6}$$

To apply the three models given by Eqs. (2) to (6), the in-plane shear strengths at glassy ($\tau_{\mathrm{LT,G}}$) and leathery ($\tau_{\mathrm{LT,L}}$) states are required. According to the test results in Table 2 the average maximum load ($P_{\mathrm{avg.max}}$) at +220^{o}C was a mere 15% of the RT value. Similarly, as the plot in Fig. 1 shows, E', from the DMA testing, gave the same reduction rate over the same temperature range. Because the shear-out failure is a matrix dominant mode it may be assumed that $\tau_{LT,L} = 0.15\tau_{LT,G}$.

The required parameters for the three models are presented in Table 3. Those for the two models by Feih *et al*. (2007) and Correia *et al*. (2013) are different for the O and B bolt types, and were obtained by curve fitting of the experimental results reported in Table 2. The kinetic parameters for the third mechanism-based model from Bai and Keller (2008) are independent of bolt type and using the DMA test results were calibrated through the modified Coats-Redfern method (Coats and Redfern, 1964, 1965). Because the six batches with blind bolting gave $P_{avg.max}$, on average, 23% lower than that of the six batches with ordinary bolted joints, it was necessary to factor $\tau_{LT,G}$ by 0.77 when predicting P_{sh} for blind bolting using the upper and lower bounds, i.e. Eqs. (4) and (6) from Bai and Keller (2008).

Figs. 10a and 10b are constructed to compare the analytical predictions from the three models with $P_{avg.max}$s taken from Table 2. Fig. 10a is for the ordinary bolting with a batch's $P_{avg.max}$ located at the centre of the solid black circle symbols. Fig. 10b is the equivalent figure for the blind bolted joints with open circle symbols for the $P_{avg.max}$s. The error bars in the figures represent one standard deviation (SD) in $P_{avg.max}$, with the SD calculated for the Gaussian statistical distribution from the three P_{max} results in a batch. Predictions for P_{sh}, using the four Eqs. (2) to (6) for $\tau_{LT,T}$ in Eq. (1), are plotted as continuous curves over the temperature range from 0 ^{o}C to +250 ^{o}C. The Feih *et al*. (2007) model results are represented by the red curve and the Correia *et al*. (2013) model by the green curve. The blue dashed curves are for upper (Eq. 4) and lower (Eq. 6) bound predictions using the model of Bai and Keller (2008).

It can be seen in Fig. 10 that the three models yield satisfactory predictions in relation to the experimental results. The superior predictions by the two models by Feih *et al*. (2007) and Correia *et al*. (2013) benefit from their modelling ability being formed from calibration of parameters by curve fitting to the same joint strength data plotted in the figures.

In order to compare the reliability of the predictions by the three models, the following statistical process was followed (taking O joints as the example). First, at a specific temperature, the ultimate joint load was predicted based on each model through Eqs. (2) to (6). Then the ratio of the prediction divided by the experimental result was calculated. Since there are 18 experimental results for ordinary bolted joints, each of the three models produced 18 ratios. Finally, using the Gaussian statistical distribution the SD and coefficient of variation (CoV) for the 18 ratios were calculated as parameters that quantified the predictive reliability of each model. The same process was followed using the results with the B joints. The statistical analysis was performed for the rule of mixtures in the Bai and Keller (2008) modelling approach, but not for the inverse rules of mixtures approximation.

For O joints the Feih *et al*. model gives a SD of 0.06 kN and CoV of 0.06, the Correia *et al*. model a SD of 0.01 kN and a CoV of 0.11. The Bai and Keller model marginally overestimates the $P_{\max}$s for the O joints by 14%, giving a SD of 0.14 kN and a CoV of 0.13. Bai and Keller (2009) have previously reported a similar overestimation when using their upper bound approximation.

For B joints the SD and CoV increase to 0.17 kN and 0.16 for the Feih *et al*. model. The same trend is found with the Correia *et al*. model, with a SD of 0.25 kN and CoV of 0.22. The upper bound solution by the Bai and Keller model yields a relatively higher SD of 0.34 kN with a CoV of 0.30. However, the empirical models require different sets of parameters (see Table 4) calibrated by curve fitting from the corresponding experimental data for O joints or B joints. Accordingly, the outcomes of these two approaches would be highly dependent on the availability and reliability of experimental data.

The parameters required in the Bai and Keller model, using either the rule of mixtures or the inverse rule of mixtures bound approximation, can be conveniently determined from a relatively small number of DMA data points by applying the modified Coats-Redfern method (Coats and Redfern, 1964, 1965). Furthermore, only one set of parameters (namely $E_{A,d}$, A_G, and n_G) needs to be calibrated, without the need for a curve fitting procedure to experimental results. In addition, the upper and lower bound curves can give the strength range that should cover the experimental strength range. Because experimental data is not always going to be available that corresponds to joint detailing to be designed in PFRP structures (Turvey, 2000; Bank, 2006), the mechanism-based model provides a rational procedure for strength prediction in PFRP structures when subjected to elevated temperatures.

It should be noted that these observations are made based on the shear-out mode, which is commonly found with single bolted joints made with relatively highly orthotropic PFRP material (Cooper and Turvey 1995; Turvey and Wang 2007b). The experimental observations and the kinetic modelling methodology presented herein can provide the basis for us to understand how temperature affects other modes and joints subjected to different loading conditions. The justification for this extension of our work is that Bai and Keller (2009) showed that the mechanical degradation of a polymeric composite laminate is fundamentally associated with a glass transition process.

CONCLUDING REMARKS

For the first time, tensile testing for strength variation was conducted on PFRP double-lap single bolted joints subjected to elevated temperatures from room temperature to +220 ^{o}C. Both ordinary and blind steel bolts were used to assemble 18 joints of each bolt type in six batches to cover characterisation over the temperature range. The thermal-mechanical responses of the 36 joints were studied by way of load-displacement curves, mode of failure and maximum (ultimate) loads. The experimental maximum loads were compared with predictions by applying three analytical models and satisfactory agreement was obtained.

Based on the current study the following conclusions can be drawn:

1) DMA and TGA test results showed that the glass transition temperature of the 5.5 mm thick PFRP plate material was in the range +143 ^{o}C to +153 ^{o}C, and that the decomposition temperature ranged from +368 ^{o}C to +399 ^{o}C; the variation was seen to be dependent on the heating rate. On the basis of the DMA and TGA data the six selected test temperatures chosen were +23 ^{o}C, +60 ^{o}C, +100 ^{o}C, +140 ^{o}C +180 ^{o}C and +220 ^{o}C, to cover glass transition and to ensure that no PFRP decomposition occurred. Within this temperature range, all ordinary and blind bolted joints failed with the shear-out mode in the inner PFRP plate. For temperatures > 100 ^{o}C it was observed that the surface colour of PFRP changed from a 'white' to a 'darker brown'; the degree of colour change increased with temperature. It is believed that surface polymer matrix oxidation in the air atmosphere was the cause of the distinct colour change.

2) Elevated temperatures were found to modify the characteristics of the load-displacement curves for both ordinary and blind bolted joints. At room temperature (+23 ^{o}C), load increased linearly with joint displacement up to the maximum load, followed by a sudden load drop to a lower level that was constant to an axial displacement of 20 mm. As the temperature was increased the load-displacement curve became increasingly non-linear before the maximum load was reached. When the temperature was higher than +100 ^{o}C it was found that the joint's load decreased gradually after maximum load as the axial displacement grew to 20 mm.

3) The average maximum load of joints (batches of three nominally identical specimens) with ordinary bolts was 15.5 kN at +23 ^{o}C. It dropped by 14%, 38% and 64% at temperatures of +60 ^{o}C, +100 ^{o}C and +140 ^{o}C. A significant reduction to 78% (3.39 kN) was obtained at +180 ^{o}C, and at the maximum constant temperature of +220 ^{o}C the average maximum load was 2.4 kN or 15% of that at room temperature. For the blind bolt the average maximum joint load at +23 ^{o}C was lower at 12.3 kN than for the ordinary bolt. For whatever reason, the strength reduction was only 3.1% at +60 ^{o}C; significantly less than with the ordinary bolting. Above +60 ^{o}C, reductions were 37%, 74% and 79% at temperatures of +100 ^{o}C, +140 ^{o}C and +180 ^{o}C, respectively. Finally, at +220 ^{o}C, the average maximum load was a mere 1.84 kN for a reduction of 85%; the same maximum reduction as obtained with ordinary bolting. It was found that the average maximum loads in batches of blind bolted joints were, on average, lower by 23%; the lower strength was caused by the slot (for blind fixing) in the steel shaft introducing a damaging stress concentration state into the PFRP plate.

4) Models leading to closed-formed equations by Feih *et al*. (2007), Correia *et al*. (2013) and Bai and Keller (2008) were studied to predict the maximum (ultimate) loads of 36 failed joints. It was shown that predictions by the three models over the full temperature range agreed well with the experimental strength results. Using the rule of mixtures and the inverse rule of mixtures approximations, the modelling by Bai and Keller (2008) gave predictions for upper and lower bounds to the joint ultimate load. The key advantage of the Bai and Keller model is that it is not semi-empirical and so calibration of parameters does not rely on curve fitting to available experimental test results. The authors therefore recommend its application when undertaking initial design calculations for the safe design of

PFRP bolted joints that are to be subjected to elevated temperatures up to the decomposition temperature.

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LIST OF CAPTIONS

Table Captions:

Table 1. Experimental results of DMA and TGA tests

Table 2. Bolted joint specimen labels and experimental results from tensile tests

Table 3. Calibrated parameters for Eqs. (2) to (5).

Figure Captions:

Fig. 1. DMA test results at a heating rate of 10 ^{o}C/min.

Fig. 2. TGA test results at heating rate of 10 ^{o}C/min

Fig. 3. Double-lap joints for tensile testing under elevated temperatures: (a) joint dimensions of unit in mm, and (b) assembled double-lap joint with single blind bolt

Fig. 4. Experimental set-up for tensile testing of PFRP bolted joints under elevated temperatures

Fig. 5. Failure modes of PFRP joints with single ordinary bolt at elevated temperatures of: (a) +23 ^{o}C; (b) +60 ^{o}C; (c) +100 ^{o}C; (d) +140 ^{o}C; (e) +180 ^{o}C; (f) +220 ^{o}C.

Fig. 6. Failure modes of PFRP joints with single blind bolt at elevated temperatures of: (a) +23 ^{o}C; (b) +60 ^{o}C; (c) +100 ^{o}C; (d) +140 ^{o}C; (e) +180 ^{o}C; (f) +220 ^{o}C.

Fig. 7. Load-displacement curves of PFRP joints with ordinary bolts under elevated temperatures

Fig. 8. Load-displacement curves of PFRP joints with blind bolts under elevated temperatures

Fig. 9. Reduced contact area for the blind bolt: (a) failed PFRP blind bolted joint with one side outer PFRP plate removed, showing the context of the joint for investigation in (b) when the inner PFRP plate was removed, it showed rovings of inner PFRP plate filling the slot of blind bolt and (c) the roving filling in the slot of blind bolt was more obvious after the blind bolt was totally removed from the joint. Both (b) and (c) indicate the contact of the slot of

blind bolt with the hole of inner PFRP plate, resulting in a reduced contact area of the inner PFRP plate.

Fig. 10. Comparisons between model predictions and experimental results for PFRP bolted joints with a single: (a) ordinary bolt; (b) blind bolt.

Tables:

Table 1. Experimental results of DMA and TGA tests

(1)	Heating rate (^{o}C/min)			
	3.0 (2)	5.0 (3)	7.5 (4)	10.0 (5)
T_g (^{o}C)	142.9	145.0	149.8	153.3
T_d (^{o}C)	367.7	389.5	394.4	399.4
Remaining mass (%)	77.4	81.9	85.8	89.0

Table 2. Bolted joint specimen labels and experimental results from tensile tests

Specimen label (1)	Target temperature T_{target} (^oC) (2)	Measured Temperature $T_{measured}$ (^oC) (3)	Max. load P_{max} (kN) (4)	Average measured temperature $T_{avg.measured}$ (^oC) (5)	Average Max. load $P_{avg.max}$ (kN) (6)	Max. load reduction (%) (7)
OR-1	23	24.2	15.01	24.3	15.5	0
OR-2	23	24.3	16.69			
OR-3	23	24.5	14.74			
O60-1	60	62.3	13.27	62	13.3	14
O60-2	60	62.5	13.27			
O60-3	60	61.7	13.36			
O100-1	100	103	8.66	102	9.65	38
O100-2	100	102	10.38			
O100-3	100	102	9.91			
O140-1	140	143	5.87	142	5.60	64
O140-2	140	143	5.47			
O140-3	140	142	5.45			
O180-1	180	180	3.67	181	3.39	78
O180-2	180	184	3.57			
O180-3	180	179	2.93			
O220-1	220	219	2.37	216	2.29	85
O220-2	220	213	2.08			
O220-3	220	217	2.41			
BR-1	23	24.3	12.78	24.5	12.3	0
BR-2	23	24.5	11.47			
BR-3	23	24.6	12.49			
B60-1	60	61.3	12.02	61	11.9	3.1
B60-2	60	61.2	11.98			
B60-3	60	61.5	11.63			
B100-1	100	102	8.48	102	7.77	37
B100-2	100	101	6.74			
B100-3	100	103	8.08			
B140-1	140	139	3.24	139	3.24	74
B140-2	140	141	3.30			
B140-3	140	139	3.18			
B180-1	180	176	2.65	177	2.56	79
B180-2	180	180	2.41			
B180-3	180	177	2.63			
B220-1	220	213	1.95	213	1.84	85
B220-2	220	212	1.72			
B220-3	220	215	1.87			

Table 3. Calibrated parameters for Eqs. (2) to (5).

Model	Parameters	
	Ordinary bolted joints	Blind bolted joints
Feih *et al*. (2007) - Eq. (2)	φ = 0.0179 $^{o}C^{-1}$, T_k = 110 ^{o}C	φ = 0.0159 $^{o}C^{-1}$, T_k = 84.1 ^{o}C
Correia *et al*. (2013) – Eq. (3)	B = -9.24, C = -0.0245 $^{o}C^{-1}$	B = -3.79, C = -0.0216 $^{o}C^{-1}$
Bai & Keller (2008) – Eq. (5)	$E_{A,d}$ = 16500 kJ.mol, A_G = 4.56, n_G = 0.61	