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<td>Author(s)</td>
<td>Fung, Tat Ching; Tan, K. H.; Nguyen, Minh Phuong</td>
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Structural Behavior of CHS T-Joints Subjected to Static In-Plane Bending in Fire Conditions

T. C. Fung¹; K. H. Tan²; and M. P. Nguyen³

Abstract: Fire resistance of steel joints is always a major concern in the design of steel structures under extremely hazardous conditions. However, for circular hollow section (CHS) joints in fire conditions, little information is available, especially for T-joints. To gain more insight into the static behavior of CHS T-joints in elevated temperatures, experimental and numerical studies were conducted on selected T-joints subjected to in-plane bending. The failure modes and ultimate strength of the joints subjected to different temperatures were investigated and compared to the corresponding joints at ambient conditions. Within the range of investigated parameters, at 700°C, the joint strength was reduced to 22.1% compared to the corresponding joint at ambient temperature. Furthermore, it is observed that at high temperatures, a change occurred in the failure mode of the joints. Cracks formed around the center weld toes before the joints reached excessive deformation, which subsequently affected the joint postyield hardening performance. To understand the initiation of the cracks, a material test was performed. The fracture strains of the heat-affected zone of the chord material beneath the center weld at corresponding temperatures were determined. The fracture strains were included in subsequent finite-element (FE) validation models. The verified FEA models were used to analyze the structural behavior of CHS T-joints at elevated temperatures, such as strain, stress, load path, and effect of fracture strains, to gain insight into the failure mechanism of the joints. DOI: 10.1061/(ASCE)ST.1943-541X.0001382.

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Author keywords: Circular hollow section; T-joint; Ultimate strength; Experimental study: fire; In-plane bending; Finite-element model; Structural safety and reliability.

Introduction

Steel tubular members are becoming prominent in structural steel design owing to their flexibility in accommodating complex geometry. As an important part of a tubular structure, joints require greater attention because of their geometric discontinuity and complexity in stress distribution at the connections. For T-joints in ambient temperature, there exists an extensive literature on joint strength. Since the first tests conducted by Toprac (1961) and Togo (1967), the behavior of T-joints were extensively studied by Kurobane et al. (1984) and Van der Vegte (1995) in the 1980s and 1990s. A database on joint behavior was first introduced by Parker (1985), Makino et al. (1996) updated the database, which reported on 127 tests and 89 numerical investigations. The database provides background information for many current design codes, such as EuroCode3, Comité International pour le Developpement et l’Etude de la Construction Tubulaire (CIDECT) (2010), and IWW (2009). To date, the database and design codes continue to be developed by many researchers focusing on four main streams: thick-walled joints (Choo and Qian 2005), joints with initial cracks (Zerbst et al. 2002), effects of chord stresses (Van der Vegte and Makino 2006), and reinforced joints (Fung et al. 2002).

Besides normal operational loads, fire is always a major concern in the design of steel structures against extreme hazard. In the early days, the protection scheme for steel structures under fire and explosion focused on requirements for increasing the fire resistance of steel structures. This was achieved by either delaying increases in the temperature of the steel or limiting the temperature rise to approximately 550°C. The disadvantages of this approach are the avoidance of understanding the actual behavior of structures in fire and the rising cost of insulation materials. Fire protection extends the construction process and increases construction costs. Robinson and Latham (1986) indicated that expenses on fire protection could contribute up to 30% of the total cost of steel structures for buildings. Recently, a performance-based fire engineering approach was proposed and used in the design of steel structures under fire as a means of satisfying the requirements of overall safety and protection schemes. This method ensures fire safety at a reasonable cost and avoids the use of excessive fire protection materials without compromising structural safety. The performance-based approach requires an understanding of the behaviors of steel frame members under fire conditions. However, most studies on steel structures at elevated temperatures have largely concentrated on isolated members (Lin and Tao 2007; Lin and Han 2005; Yang and Han 2005; Zhao et al. 2005), while a few have dealt with steel tubular T-joints. Yu et al. (2011) presented an experimental study on the mechanical behavior of an impacted steel tubular T-joint in fire. Jin et al. (2011) conducted a parametric study on the mechanical behavior of steel planar tubular trusses in fire to investigate the failure modes, temperature distribution, and load-bearing capacities. However, the failure mechanism and associated ultimate strength of individual circular hollow section (CHS) T-joints at elevated temperatures subjected to a static load have not been investigated.

In an actual fire incident, the thermal expansion of steel members in tubular structures will cause additional axial compression...
stress to the connection. The effect of this thermal-induced compression was considered independent of the failure mechanism of the joint as a similar concept in current design codes, such as CIDECT (2010). In this paper, experimental and numerical studies are carried out to investigate the structural behavior of CHS T-joints subjected to in-plane bending in fire conditions, with a focus on the joint mechanical behavior and its failure mechanism. An isothermal heating test procedure was selected for this study, and axial restraint due to thermal expansion was released where the joints were heated to the desired temperatures and then subjected to static load until failure occurred. This testing procedure served the objective of the study because it produced no side effects, such as thermal restraint or a fast/slow increasing temperature rate.

To further investigate the load-transfer mechanism in corresponding load and fire conditions, finite element (FE) models of the joints were also developed in this study. The effect of elevated temperature on steel joints was examined by incorporating temperature-dependent material properties in accordance with EC3:P1-2 (2005a). The behavior of CHS T-joints subjected to high temperatures was investigated based on data obtained from the test program and a parametric study based on the validated FE models. The results provide valuable data for studying the behavior of T-joints subjected to high temperatures.

97 Test Program

98 Description of Test Specimens

Fig. 1 shows the configuration of a typical CHS T-joint and definitions of the main geometric parameters. In this study, the chord diameter of all joints was designed to be constant at 244.5 mm, with the brace-to-chord diameter ratio \( \beta = d/D \) varying from 0.2 to 0.8. The brace length was kept at six times the largest brace diameter to allow brace bending to be the dominant joint’s failure mode.

The chord length-to-radius ratio \( \alpha = 2L/D \) was 18 to avoid short chord effects (van der Valk 1988), and the brace-to-chord thickness ratio \( \tau = t/T \) was 1.0, with a chord thickness of 6.3 mm to preclude local failure at the brace. The steel grade for the entire population of test specimens was EN 10210 S355J2H, with a characteristic yield strength \( f_y \) of 355 N/mm\(^2\) and an ultimate strength \( f_u \) of 510 N/mm\(^2\) (steel grade).

This paper presents test results from five full-scale T-joint specimens. The chosen specimens are highlighted in Table 1 from 25 investigated cases including 5 tests and 20 numerical analyses. The tests are used to understand joint behavior at high temperatures and then as a benchmark for numerical validations. The tested specimens were a combination of three levels of temperature—20, 550, and 700°C—and three levels of \( \beta = 0.47, 0.69, \) and 0.79. The range of \( \beta \) was chosen within the practical design limit. These levels of temperature were selected because steel properties would change significantly between these levels, as specified in EC3: P1-2 (2005a). More specifically, at 20°C, steel properties do not change; at 550°C both the yield strength and stiffness are reduced significantly; lastly, at 700°C steel almost entirely loses its strength. The nomenclature of each specimen in the combination is presented in Table 1. The specimens were named with reference to their loading (IB for in-plane bending), the type of joint (T), the value of \( \beta \), and, finally, the temperature at which they were tested.

99 Test Setup

The isothermal heating test is a procedure in which a specimen is heated to a specified temperature and then tested at that constant temperature. This testing process was performed in an electric heating furnace with an internal volume of 1 m\(^3\). The interior maximum temperature can reach 1,100°C and remain at that temperature for 3 h. The temperature uniformity within the furnace was first confirmed by a trial test in which 18 thermal couples were installed around the furnace. Thermocouple readings showed that the temperature of the insulated furnace interior was rather uniform with a standard deviation of \( \pm 20°C \) at the maximum temperature.

The test setup is shown in Fig. 2, where CHS T-joints are placed in the middle of the furnace. On each side were two A-frames acting as external supports. The chord was extended outside the furnace, and its two ends were fixed to the upright column flanges of the A-frames by sixteen 30-mm-diameter bolts. To release thermal restraint during the heating process, one of the chord ends was laid on a roller support. When the temperature at the joint center had reached a stable state, this roller support was tightened to the A-frame as a fixed end condition.

In-plane bending was applied to the joint through a system as shown in Fig. 2. Compression force was generated by a 100-t hydraulic jack placed at the center of the reaction frame. The load was transferred to the top of the brace via a pin–pin rod that provided load stability throughout the test with a loading rate of 1–2 mm/min as it generated a static strain rate of \( 10^{-3} \) mm/s. The dimensions of the furnace opening at the top are large enough to allow for lateral deflection of the brace under bending.

To minimize heat loss and provide a safe testing environment, all the extensions of the joint beyond the furnace were wrapped...
Table 1. Study Series of CHS T-Joint under In-Plane Bending

<table>
<thead>
<tr>
<th>T1:2</th>
<th>Loading condition</th>
<th>Temperature (°C)</th>
<th>Nomenclature</th>
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<tbody>
<tr>
<td>T1:4</td>
<td>In-plane bending</td>
<td>20</td>
<td>IB.T.079.20</td>
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<tr>
<td>T1:5</td>
<td></td>
<td>200</td>
<td>IB.T.079.200</td>
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<td>T1:6</td>
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<td>400</td>
<td>IB.T.079.400</td>
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<tr>
<td>T1:7</td>
<td></td>
<td>550</td>
<td>IB.T.079.550</td>
</tr>
<tr>
<td>T1:8</td>
<td></td>
<td>700</td>
<td>IB.T.079.700</td>
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</tbody>
</table>

The T-joints were tested within a well-insulated furnace. Therefore, it is harder to place instrumentation around the joint behavior compared to a conventional ambient test. However, all available instrumentation was designed to maximize the accuracy of joint-behavior measurements.

For those parts of the joint outside the furnace, deflections were captured by LVDTs at six positions from L1 to L6 (Fig. 3). The support rigidities on two sides of the chord were confirmed by four LVDTs, i.e., L1 to L4. Support rotations were calculated based on (rad) and

\[ \theta_{app1} = \frac{L1 - L2}{450} \text{(rad)} \]

and movements are

\[ \Delta_{app1} = L1 + L2 \text{ (mm)} \]

\[ \Delta_{app2} = L3 + L4 \text{ (mm)} \]

During the test, it was observed that the two supports moved within 0.12 mm toward the brace and rotated 0.002 rad (0.36°) accordingly. Therefore, it may be concluded that the boundary conditions of the two chord ends were fully fixed. The purpose of L5 and L6 is to measure the rotation of braces, which can be calculated as

\[ \theta_{b1} = \frac{L5}{1,000} \text{ and } \theta_{b2} = \frac{L6}{800} \text{ (rad)} \]

where \( \theta_{b1} \) and \( \theta_{b2} \) = rotation of brace corresponding to locations of LVDTs L5 and L6, respectively. The two rotations are then used to check the rigid rotation of the brace by comparing \( \theta_{b1} \) and \( \theta_{b2} \) with regard to the elastic rotation of the brace as a cantilever ( \( \theta_{bE} \) ). It was concluded that the brace act rigidly, and the rotation of the individual joint was calculated based on

\[ \theta = \text{average}(\theta_{b1}, \theta_{b2}) - \theta_{g} \text{ (rad)} \]

where \( \theta \) = rotation of joint in moment-rotation curves.

Note: All cases have an identical chord diameter (244.5 mm), and a chord thickness (6.3 m), \( \alpha = 18 \) and \( \tau = 1 \).

*Test cases; the others are numerical investigations.

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Measurements of the applied load were recorded through a load cell placed at the top of the brace. The capacity of the load cell was 100 kN. The moment applied to the center joint was calculated based on

\[ M = 1.3P \text{ (kNm)} \]

where \( P \) = load measurement by the load cell (kN).

Another measurement was the profile of strain variations around the brace–chord junction. Normal strain gauges can only provide reliable results within 100°C. Therefore, only Specimen IB.T.069.20 had normal strain gauges. For the other specimens, strain measurements were obtained through high-temperature heat-resistant strain gauges. These gauges could provide high-accuracy readings up to a temperature of 1,100°C. Note that at elevated temperatures, the strain-gauge readings included the thermal-expansion strain of steel material. Therefore, a material thermal test was performed in which specimens were heated to 700°C and allowed to expand freely. The recorded free-thermal-expansion strain was first compared with EC3 for validation. The comparison study showed good agreement with EC3 predictions with a maximum discrepancy of 5%. The free-thermal-expansion strain was then subtracted from the total strain measured from the structural test.

Nine Type K mineral insulated thermocouple wires were used to measure the temperatures on the specimens as well as the furnace interior. The thermocouples were arranged across the section of the specimens in such a manner that the temperature at the joint region could be captured.

### Imperfection Measurement

Since the structural steel tests are sensitive to imperfections, measurements were performed on the joints after their erection on the test frame. Because joint failure took place beneath the brace–chord junction in the chord region, the geometrical information of the joint and the quality of welds were important. However, following analysis of the test results, the joint imperfections were found to have minor effects and to have no effect on the overall behavior of the joints. Hence, they were not reported in this paper.

### Material Properties

A material test was conducted to determine the chord strength at three different levels according to the actual recorded temperatures of the structural tests. The material tensile test results are summarized in Table 2.

In the structural tests at 550 and 700°C, steel fractures occurred along the center weld toes and penetrated through the chord thickness. Hence, a material test was conducted to determine fracture strains of the material in this heat affected zone (HAZ). The tensile coupon was designed to simulate the same effect on the parent material from a welding process interior. As shown in Fig. 4, the coupon was covered with the same volume per unit length of weld material in order to generate the same amount of heat as in the structural joint. The coupon was then air cooled. To control the fracture of the coupon in the zone of interest, the welds were carefully ground off so as not to affect the fracture toughness of the parent material, and two notches were made at the center. The tensile fracture test was performed at three temperature levels, 20, 550, and 700°C, with a loading rate of 1 mm/min. Stress–strain relationships were obtained through a load cell and an extensometer (Fig. 4). The true stress and strain relationships prior to fracture were determined based on the reduced section of the coupon at the fracture stage:

\[ \varepsilon_{pl}^f = \ln \left( \frac{A_0}{A_f} \right) \]

where \( A_0 \) = nominal initial area of cross section; \( A_f \) = final area of cross section after fracture. The test properties of the HAZ material are summarized in Table 3.

---

**Table 2. Material Tensile Test on S355 Steel Material**

<table>
<thead>
<tr>
<th>Coupon</th>
<th>Test temperature (°C)</th>
<th>Yield strength (MPa)</th>
<th>Elastic modulus (GPa)</th>
<th>Ultimate strength (MPa)</th>
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<tr>
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<td>01-03</td>
<td>27.4</td>
<td>380.3</td>
<td>201.2</td>
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<td>T2:2</td>
<td>02-04</td>
<td>526.7</td>
<td>270.0</td>
<td>111.2</td>
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<tr>
<td>T2:3</td>
<td>03-05</td>
<td>680.3</td>
<td>99.5</td>
<td>26.2</td>
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</table>

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**Fig. 4.** Fracture test of heat-affected material: (a) addition of weld; (b) subsequent removal of weld and addition of notches
Table 3. Material Tensile Test on Heat-Affected Material

<table>
<thead>
<tr>
<th>Test</th>
<th>Initial fracture strain, ε_p</th>
<th>Ultimate stress (MPa)</th>
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<tr>
<td>T3:1</td>
<td>01 25.3 0.521 0.476 580.3</td>
<td></td>
</tr>
<tr>
<td>T3:2</td>
<td>02-05 25.3 0.370 0.353 651.2</td>
<td></td>
</tr>
<tr>
<td>T3:3</td>
<td>06-09 539 0.230 0.249 342.1</td>
<td></td>
</tr>
<tr>
<td>T3:5</td>
<td>10-13 698 0.180 0.143 71.61</td>
<td></td>
</tr>
</tbody>
</table>

*T3:4 06-09 539 0.230 0.249 342.1
*T3:3 02-05 25.3 0.370 0.353 651.2
*T3:2 01 25.3 0.521 0.476 580.3
*T3:1 Coupon
*T3:5 10-13 698 0.180 0.143 71.61

261 Test Procedure and Observations

262 Test Procedure

263 The test consisted of two phases: a heating phase and a loading phase. In the first phase, the furnace was heated up at a heating rate of 100–110°C/min up to the specified temperature. The heating rate was similar to that of a natural fire (ISO 834 fire curve) in its first 5 min. When the furnace air reached the test temperature, it was maintained to the end of the test. During the first phase, thermal expansion was observed on the chord. However, the expansion did not cause any stress in the joint since the chord ends rested on a roller. This was validated by two thermal strains attached on two sides of the chord middle plane. The average of S1 and S2 is shown in Fig. 5, which shows how the strains fluctuate with amplitude of 0.002 for the first 15 min, indicating that the stress due to thermal expansion is negligible.

264 When the center of the joint reached the preset temperature, the test entered its second phase. Gaps on the chord ends were filled with steel plates, and eight bolts on the end plate were tightened. A small load was applied to check the adequacy of the test setup, instrumentation, and loading system. In this phase, a load was applied until the maximum stroke of the hydraulic jack was reached. In the fire test, cracking sounds were heard when the joint reached a certain load. After the cracking sounds, the applied load started to decrease.

265 Temperature Distribution

266 The development of temperature on the chord is illustrated in Fig. 6 for Specimen IB.T.069.550. The joint temperature distribution is divided into three zones based on the reading of nine thermal couples. It was observed that the center zone took 13.2 min to reach the specified temperature after the air temperature had stabilized. This duration was allowed for before starting the loading phase. It can also be observed that, during the loading phase, the temperature difference in each zone is within a range of 3–8%. The same variation was found for all tested T-joints. However, the joints could only reach 526.3°C since there were parts of the joints extending outside of the furnace. That is also why in Zone 3 (Fig. 3) the temperature evolved the slowest compared to the other two zones. Based on the measured temperature of the four tested joints, the temperature distribution in three zones can be used as the temperature inputs in the analytical and numerical analyses.

267 Failure Modes

268 The failure of CHS T-joints subjected to in-plane bending occurred on the chords in all five joint tests. As the brace rotated, local bending and shearing occurred on the chord near the crown points. Areas with large plastic deformation were observed around the chord wall near the crown positions. However, the chord material inside the brace section did not undergo much deformation. After the test, the center section was mechanically cut out from the joint for more detailed observation.

269 The failure modes of the joints at ambient and elevated temperatures are shown in Fig. 7. On the compression sides of all connections, a similar failure mechanism was observed. Large plastic deformations were formed beneath the weld toe, indicating a concentration of stresses. However, on the tension sides of connections, for all joints at elevated temperatures, there were cracks along the weld toe, whereas no cracks were found in the ambient test. In each joint at elevated temperatures, a single crack started from the crown point and developed toward the saddle points and penetrated through the chord thickness (Fig. 7, IB.T.069.550 tension side). The cracks caused a significant difference in failure mechanisms between high-temperature and ambient-temperature cases. In the high-temperature cases, the joint strength did not increase after the cracks formed.

As illustrated in Fig. 8, crack lengths varied among joints. For IB.T.069 at 550 and 700°C, higher temperature led to longer crack lengths. In addition, at the same temperature (550°C), the larger the brace, the longer the crack.

270 Moment-Rotation Curves

271 Figs. 9(a and b) show the moment-rotation curves of all five CHS T-joints. They are classified into two categories for comparison purposes. Fig. 8(a) shows the behavior of Joint IB.T.069 at three levels
of temperature, 20, 550, and 700°C, in order to allow observation of the effect of temperature on an individual joint. The moment-rotation curve of Joint IB.T.069.20, which was tested in ambient temperature, is similar to typical joint behavior reported by previous researchers (Togo 1967; Van der Vegte 1995). As the load increased, the joint deformed linearly up to the yield value (30.88 kNm) and the strength started to increase continuously until the test was stopped. There was no failure on the joint. In this case, Yura's deformation limit ($80\sigma_y/E = 0.135$ rad) was adopted to find the ultimate joint strength. The same criteria were mentioned in Makino et al. (1996). However, when the temperature was increased to 550 and 700°C, the joint started to yield earlier, i.e., 23.67 kNm at 550°C and 6.74 kNm at 700°C. When deformations reached 0.082 rad and 0.035 rad, respectively, the joints could not take any more loads. Hence the highest load was considered to be the ultimate strength of joints at high temperature. It was also noted that cracking sounds were made at the moment when joints reached their maximum loads. In these cases, Yura's deformation limit was not applicable because the maximum load capacity was reached before Yura's deformation limit, despite the fact that the material reduction was considered in the formula $(80\sigma_y/E)$ ($k_{\gamma,\theta}/k_{E,\theta}$), where $k_{\gamma,\theta}$ and $k_{E,\theta}$ are the reduction factors of elastic modulus at temperature $\theta$. It can be seen that the capacity of the joint is governed by the shear strength of the material at the HAZ (Fig. 7) at elevated temperatures instead of deformation control at ambient temperature.

Results of the test are presented in Table 4. Compared to the ambient-temperature case (IB.T.069.20), the joint strength was reduced to 77.2% at 550°C and 22.1% at 700°C. Owing to the deterioration of the steel, the initial stiffness changed as well. However, at 550°C, the initial stiffness of the joint was only reduced by 3%, in contrast to 53% at 700°C.

In Fig. 9(b), at 550°C, the behavior of joints with different $\beta$ is illustrated to show the effect of $\beta$ at higher temperatures. The initiation of flexural softening behavior occurred together with fracture in the HAZ. This joint flexural softening behavior was different for different joints. For a joint with large $\beta$ (IB.T.079.550) the reduction was greater compared to those with smaller $\beta$ (IB.T.069.550 and IB.T.047.550). The rate of joint flexural softening indicates greater crack development, which is related to the applied load and mechanical behavior of the chord at high temperatures. For the smallest $\beta$ (IB.T.047.550), this process happened...
slowly from a rotation of 0.056 to 0.097 rad. This is due to the different load levels of the three joints. Joint IB.T.047.550 was subjected to 12.4 kNm moment when cracks initiated. At this load level, slow crack propagation was observed. Meanwhile, Joint IB.T.079.550 sustained a greater moment of 35.88 kNm at fracture, with cracks quickly propagating to the saddle owing to the negligible hardening properties of steel at high temperatures [EC3:Pt.1.2 (CEN 2005)]. The responses agreed well with crack-length variations among these joints.

The ultimate strengths of the tested T-joints with different $\beta$ were compared with calculations based on design codes. In this study, CIDECT’s design formula (Wardenier et al. 2010) was adopted because of its popularity in practical tubular design. This is a semi-empirical formula based on the database of tubular joint strength established by Makino et al. (1996). Although the design criteria for CIDECT are for joints in ambient conditions, the design formula is based on $f_{y0}$, the characteristic yield strength. Hence it might be used for high temperatures by modifying the yield strength $f_{y0}$ to $f_{yT} = k_{yT} \times f_{y0}$, where $k_{yT}$ is the reduction factor of yield strength due to elevated temperature. Values of $k_{yT}$ are obtained from Table 3:

$$N_{1,Rd} = 4.3 f_{y0} T^2 D \sin 90° \beta \gamma^{0.3} Q_f$$

where $f_{y0}$ = characteristic yield strength under ambient conditions; $T$ = chord thickness; $D$ = outer diameter of braces; $\beta$ and $\gamma$ = respectively brace-to-chord ratio and chord thickness ratio; and $90°$ = incline angle of brace to chord for T-joint. $Q_f$ takes into account the effect of preloads in a chord. In this case, since there were no chord preloads, $Q_f = 1$.

A comparison of test results and CIDECT predictions is presented in Table 4. In terms of ultimate strength prediction, the comparison shows good agreement in ambient conditions. At 550°C, the formula was conservative in predicting strength. However, at 700°C, it gave a nonconservative prediction, although deterioration of yield strength was considered.

### FE Models

#### Element Types and Failure Criteria

Finite element (FE) models of the T-joints subjected to static in-plane bending in fire conditions follow the modeling scheme of a previously published study of axially loaded CHS T-joints under elevated temperatures (Nguyen et al. 2010) because of its numerical efficiency in predicting joint behavior. In this study, the joints were simulated using ABAQUS v6.9, which consists of an eight-node doubly curved thick shell element with a reduced integration scheme (S8R). Five layers of a Gaussian integration point were generated across the chord and brace thicknesses. Fix supports were simulated at both ends of the chords, and load control was applied on the top of the braces. A typical FE model of a joint is shown in Fig. 10(a).

In FE modeling, engineering stress–strain relationships of steel and welds at high temperatures following EC3: Parts 12 and 1-8 (CEN 2005). The characteristics of yield strength and elastic moduli at various temperatures were obtained from the material

### Table 4. Test Results

<table>
<thead>
<tr>
<th>Specimens</th>
<th>Test temperature (°C)</th>
<th>Yield strength (kNm)</th>
<th>Ultimate strength (kNm)</th>
<th>Initial stiffness (kNm/rad)</th>
<th>Prediction (kNm)</th>
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<tr>
<td>T4:1</td>
<td>27.6</td>
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<td>1.15</td>
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</table>

*Ultimate strength prediction following CIDECT (2010) for high temperature, yield stress $f_{y0}$ was changed according to yield stress at high temperature.*

![Fig. 10. Finite-element model of CHS T-joint: (a) structural joint; (b) failure criteria](image-url)
tests. Ramberg–Osgood’s power law (Ramberg and Osgood 1943)
was used to convert engineering stress–strain curves into equivalent
true stress–strain curves, so that they could be adopt in the FE
models.

Note that the failure of the joints at high temperatures is the
development of cracks along the weld toes. Hence, a particular em-
phasis is placed on how to accurately model ductile fracture initia-
tion within this joint area. Without failure criteria in FE analysis,
the ultimate strength of the joints at high temperatures cannot be
simulated. In this study, a damage evolution model for ductile
material in ABAQUS v6.9 was adopted with material properties
determined from tensile coupon tests. Fig. 10(b) illustrates the
classical stress–strain behavior of a material undergoing
damage. In the context of an elastic-plastic material with isotropic
hardening, the damage manifests itself in two forms: softening of
the yield stress and degradation of the elasticity. The solid curve in
the figure represents the damaged stress–strain response, while the
dashed curve represents the response in the absence of damage. In
the figure, \( \sigma_f \) and \( \varepsilon_{pl}^{f} \) are the yield stress and equivalent plastic
strain at the onset of damage, and \( \varepsilon_{pl}^{f} \) is the equivalent plastic strain
at failure, that is, when the overall damage variable reaches the
value \( D = 1 \). The overall damage variable, \( D \), captures the com-
bined effects of all active damage mechanisms and is computed
in terms of the individual damage variables. The value of the equiva-
\( \sigma_f \) and \( \varepsilon_{pl}^{f} \) are the yield stress and equivalent plastic
length of the element and cannot be used as a material parameter
for the specification of the damage evolution law. Instead, the
damage evolution law is specified in terms of equivalent plastic
displacement.

When material damage occurs, the stress–strain relationship no
longer accurately represents the material’s behavior. Continuing to
use the stress–strain relationship introduces a strong mesh depend-
ency based on strain localization, such that the energy dissipated
decreases as the mesh is refined. A different approach is required
to follow the strain-softening branch of the stress–strain response
curve. Hillerborg et al.’s (1976) fracture energy proposal is used to
reduce mesh dependency by creating a stress-displacement re-
ponse after damage is initiated. Using brittle fracture concepts,
Hillerborg et al. defined the energy required to open a unit area
of a crack as a material parameter, \( G_f \). With this approach, the soft-
ening response after damage initiation is characterized by a stress-

The implementation of this stress-displacement concept in the
FE analysis requires the definition of a characteristic element,
\( L \), associated with an integration point. For shell elements,
\( L \) is a characteristic length in the reference surface. This definition
of the characteristic length is used because the direction in which
fracture occurs is not known in advance. In this study, the value of \( L \)
is 5 mm, similar to the value of the mesh size at the center of the
joint. The fracture energy is then given as

\[
G_f = \int_{\varepsilon_{pl}^0}^{\varepsilon_{pl}^f} L \sigma_f d\varepsilon_{pl} = \int_0^{\varepsilon_{pl}^f} \sigma_f du_{pl}
\]

This expression introduces the definition of the equivalent
plastic displacement, \( u_{pl}^f \), as the fracture work conjugate of the
yield stress after the onset of damage (work per unit area of the
\( \varepsilon_{pl}^f \) is the value of \( \varepsilon_{pl}^f \) determined by material
testing.

In this study, the value of \( \varepsilon_{pl}^f \) is a major factor that controls the
ultimate strength of the joints at high temperatures. Fig. 12 shows
the sensitivity of values \( \varepsilon_{pl}^f \) in modeling the behavior of Joint
IB.T.069.550. Therefore, material tensile fracture tests were con-
ducted to provide the strain benchmark. FE analysis is used to de-
termine the value by fitting the load-extension curve in the material
test. Note that the damage evolution model is mesh dependent.
Therefore, in FE material calibration, a mesh size similar to that in
models of structural joints was used. The appropriate \( \varepsilon_{pl}^f \) for
different temperature is summarized in Table 3. A sample of fitting the
FE model and experimental results are shown in Fig. 11.

Validations against Test Results

Figs. 13(a and b) present the numerical and experimental moment-
rotation curves of all structural tests of T-joints subjected to static
in-plane bending in fire conditions. Comparisons between the
moment-rotation curves and the failure modes of test results
and FE predictions show that the numerical and experimental re-
sults match well. It can therefore be suggested that the proposed
FEA model is able to predict well the structural behavior of the
columns.
Discussion

Failure Mechanism

Joint failure occurred on chords beneath the brace and center welds for all five tests. Plastic deformations were mostly located on two crown points. However, as a result of acting in-plane moment, one side of a joint was under compression while the other side was under tension.

The failure mechanism can be divided into three stages. Initially, the whole joint behaved in an elastic range in which stresses were highly concentrated in two crown points and spread slowly toward the saddle points. As shown in Fig. 14(a), at this stage, the major actions in the area were shear and bending moments.
This can be explained by two reasons. First, it was due to the absence of steel material strain hardening at high temperature [EC3: Part 1.2 (CEN 2005)]. Consequently, the yield part of the joint had practically no rigidity, and material softening occurred at an earlier stage. Secondly, because the steel fractured earlier at high temperatures, the development of tensile membrane action was stopped sooner. Therefore, the hardening process was much shorter at 700°C compared to the 550°C case. With regard to the loss of material strength at a high temperature, the ultimate strength of the joint was affected not only by the degradation of steel material but also by the fracture strain of the HAZ. In terms of strength, the joints at higher temperatures attained a plastic behavior earlier owing to the nonlinearity of the material stress–strain relationship at elevated temperatures and the crack developments around the center welds.

### Influence of Brace Size at High Temperature

The effect of the brace-to-chord diameter ratio ($\beta$) at one elevated temperature (550°C) is also shown in Fig. 15. The reduction in joint strength is normalized with its strength in ambient conditions. The joint strength in ambient conditions is based on validated FE models. It is observed that the strength variation ($\mu$) of different $\beta$ joints at a high temperature (550°C) is less than that in ambient conditions. In ambient conditions (continuous line), the strength shows linear variation but not at high temperatures. For a larger brace, the strength reduction is higher compared to a smaller brace. This is because the effective yielded area beneath the joint is smaller for smaller braces ($\beta = 0.47$) compared to a larger one ($\beta = 0.79$). Because the temperature directly affects material strength, the larger the effective area under a brace, the greater the strength reduction.

### Conclusions

In this paper, the findings of an experimental program to investigate the behavior of CHS T-joints subjected to in-plane bending in fire are presented. It was observed that at high temperatures, the joint failure criterion was changed from serviceability to strength owing to cracks occurring on the weld toes at high temperatures. It was also found that temperature has a significant effect on joint strength reduction. In addition, the five investigated joints, the effect of temperature increased as the braces became smaller.

In addition to the five tested specimens, numerical models of T-joints in various temperature ranges were analyzed based on the commercial package. The predictions of numerical models were validated against the test results. FE models were used for numerical study of the behavior of joints in future research.

### Acknowledgments

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References


Queries

1. Please provide author titles (e.g., Professor, Director) and for both affiliation footnotes.

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4. In the abstract, please use the full term instead of "FEA."

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6. The citation (Parker 1985) mentioned in this sentence is not present in the References list. Please provide the full details for (Parker 1985), and we will insert it in the References list and link it to this citation.

7. There is no ref. by CIDECT (2010). Do you mean “Wardenier et al. 2010”? If so, please insert “Wardenier et al.” before “2010” in parentheses. Otherwise, please provide a ref. for CIDECT.

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9. The citation (Robinson and Latham 1986) mentioned in this sentence is not present in the References list. Please provide the full details for (Robinson and Latham 1986), and we will insert it in the References list and link it to this citation.

10. The meaning of “The effect of this thermal-induced compression was considered independently of the failure mechanism of the joint as a similar concept in current design codes, such as CIDECT” is unclear. First, it is unclear whether “independent” should be changed to “independently.” If it relates to “mechanism,” meaning “it was considered to be independent of the failure mechanism,” then it’s fine, though “to be” should be inserted. If it means how it was considered (or treated), then please change to “independently.” The last part of the sentence (starting at “as a similar concept”) is also unclear. Please revise.

11. Please provide reference for this standard.

12. In Table 1, please provide a note explaining the significance of the bold font.

13. Please provide

14. Please provide name and location of manufacturer of Nicrothal wires.

15. In "Normal strain gauges can only provide reliable results within 100°C," the use of "within" is unclear. Do you mean "up to" or "at"? "Within" can be used in a range, not with a single temperature. Please revise.

16. In “eight bolts on the end plate were tightened,” if there are only eight such bolts, please insert “the” before “eight” to indicate that.

17. In Table 4 footnote, should “for high temperature, yield stress” be “for high-temperature yield stress” or “for high temperature and yield stress” or something else? The original is unclear. Please revise.

18. Please provide a reference for ABAQUS v6.9.

19. The meaning of “the failure of the joints at high temperatures is the development of cracks along the weld toes” is unclear. The problem is with “is.” Do you mean “is the result of”? Or “is illustrated by” or “is shown by”? Please revise.

20. In “…the proposed FEA model is able to predict…” please use the full term instead of “FEA.”

21. In “The stress concentration area was released to zero near the cracks,” the meaning of “released” here is unclear. Do you mean “reduced”? Please revise.
22. In “and the postyield hardening response occurred in short range,” the meaning of “in short range” is unclear. Do you mean “shortly thereafter”? Please revise.

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