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Cyclic behaviour of double-tube buckling-restrained braces for boiler steel plant structures

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Abstract: An inner-stiffened double-tube buckling-restrained brace (BRB) is proposed for boiler steel plants in the low-to-medium seismicity regions. The test specimen is of 15.0 m length, consisting of a 15.0 m inner tube with inner stiffeners at both ends and a 12.2 m outer tube for lateral deformation restraint. The full-scale test was conducted to reproduce the cyclic behaviour of the proposed double-tube steel BRB in the plant structures at a practical manufacturing level. The structural performance of the proposed BRB under extremely low cycle fatigue (ELCF), including the strength, deformation capacity, hysteretic response and the failure mode, was investigated. The finite element (FE) model incorporating a material fracture model was established and verified, which could predict the fracture initiation and propagation accurately. The parametric study was carried out to further assess the influence of the stiffener arrangement, material property, gap size and controlling metric of the imperfection on the cyclic behaviour of the proposed BRB. The experimental and numerical investigations demonstrated that the proposed braces could achieve a stable and saturated hysteretic curve. The failure mode was the stress concentration induced fracture which initiated at the inner surface of the inner tube at the stiffener tips. On the basis of the parametric study, the optimized BRB could achieve a high equivalent ratio of critical viscous damping at the loading amplitude of $1/75L$.

Keyword: Stiffened double-tube, Buckling-restrained, Extremely-low cycle fatigue, Hysteretic response, Fracture

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1. Introduction

Steel concentrically braced frames are prevailing in buildings and infrastructures serving as the lateral-action resisting system. The brace acts as an energy-dissipation member through yielding at the tensile loading and buckling at the compressive loading while providing the lateral action resistant. However, the buckling of bracing members is inevitable during the major earthquake. It can cause the deteriorations of the strength and stiffness especially during the compressive hysteresis excursions, compromise the energy-dissipation ability and ultimately lead to the premature fracture. The un-symmetrical performance under tensile and compressive loadings and the unexpected failure of bracing members can cause the unfavourable load re-distribution within the members of the frames, the storey-drift concentration and the increased risk of structure collapse. Therefore, the buckling restrained brace (BRB) based on the motivations of restraining the brace buckling to achieve a symmetrical performance under both tensile and compressive loading conditions, was proposed and investigated in 1970s [1-3].

During past few decades, the buckling-restrained braced frames have been widely adopted in the high-rise buildings and infrastructures in the high seismicity regions, owing to the satisfactory seismic performance. Various types of BRBs have been developed, which can be catalogued as the mortar restrained-system [4-7] and the plain steel restrained-system [8-13]. Compared with the former mortar-restrained BRBs, the plain steel ones are friendly for the post-earthquake inspection and the re-use of the buckling-restraining elements [13]. The different types of the core members for the plain steel BRBs, i.e. tubular [8-12] and non-tubular forms [13], have been proposed and investigated in the previous research. The hollow tubular-core BRBs, consisting of double or triple overlapped tubes, present the advantages in the fabrication and the capacity to self-weight ratio compared with the counterpart one, especially when adopted in the large-scale structures with the dimension larger than 10 m,

such as the steel frames in the thermal power plants.

The structure of the boiler workshop in the thermal power plants are of significance and the safety of the steel structures should be ensured to enable the non-interrupting or promptly resilience of the electricity supply after strong earthquakes. However, due to the special requirements of the thermal power generation reacting furnace and the corresponding arrangements of the pipelines and the affiliated equipment, the plant structures generally exhibit horizontal and vertical irregularities with unevenly-distributed gravity loads. Furthermore, the large dimension members and spans are commonly adopted in the plant structures, considering the practical issues such as minimizing steel usage, reducing the transportation times, and optimizing the construction plan. Therefore, the lengths of bracing members generally range from 10 m to 25 m, even longer in a large-scale thermal power plant. Under these circumstances, the conventional bracing members may fail to meet all the requirements while having a reasonable and economical member dimension for the fabrication, transportation and installation. In addition, the limited out-of-plane space in the boiler plants can also hinder their applications in the boiler plant structures. Previous research [14-15] showed the significantly improved seismic performance of the thermal plant structures after incorporating BRBs. This superior performance can be secured providing that the large-scale BRBs can behave satisfactorily. Due to the large dimension, the increasing difficulties of the fabrication and contribution could be the predominant issues to affect the seismic performance of BRBs. However, scarce research was performance on the large-scale BRB (longer than 12.0 m).

A stiffened double-tube buckling-restrained brace (SDT-BRB) is proposed for the application in boiler steel plants in this study. A full-scale test on a 15-m SDT-BRB specimen was conducted to investigate the influence of the large-scale size and the corresponding practical issues on the performance of the proposed BRB. Finite element (FE) analyses were employed to further assess the effects of the stiffener arrangement, material, gap size and

imperfection on the hysteretic response and ductility capacity of the proposed BRB. Finally, the recommendations to achieve satisfactory hysteretic performance were given for the practical design.

2. Experimental investigation

2.1 Test specimen

A 15-m full-scale stiffened double-tube BRB was fabricated by two cold-formed circular hollow section (CHS) tubes. The inner and outer tubes were manufactured by the cold-form process and welded by one straight seam weld. The schematic view of the test specimen is shown in Fig. 1(a). The diameters and thicknesses for inner ($D_i \times T_i$) and outer tubes ($D_o \times T_o$) were 480 mm $\times$ 14 mm and 550 mm $\times$ 25 mm, respectively. The total lengths of inner and outer tubes were 15.0 m and 12.2 m, respectively. Eight longitudinal inner stiffeners with the thickness of 25 mm were welded at both ends in every 22.5 degree, as shown in Fig. 1(a). In addition, six 45°-circular-arc plates with thickness of 14 mm were welded to the inner stiffeners within the region of 1900 mm, to ensure the lateral stability of each stiffener. The inner and ring stiffeners were designed to prevent the local buckling of the inner tube beyond the outer tube while being in the elastic stage during the test. The gap between the ring stiffeners and the inner tube as well as the gap between the inner and outer tubes were both 10 mm. Four M20 bolts were used to align the inner and outer tubes at the mid-length at each 90°, as a construction measurement. Eight 3-mm-thick rubber strips with the width of 1/16 circumference of the inner tube were mounted on the external surface of the inner tube-wall within the contact region of these two tubes. The rubber strips could mitigate the hard contact between two tubes.

The detailed information and assembling of each component are described in Figs. 1(a) and (b). The test specimens were made of Q235 steel (nominal yield strength of 235 MPa). The mechanical properties of the yield strength, the ultimate strength and the Young's

modulus derived from tensile coupon tests from the reference tube sample were list in Table 1.

2.2 Test set-up

Fig. 2(a) presents the horizontally self-balanced test set-up. Two ends of the test set-up, A and B, were connected by four stock square hollow section beams with vertical baring members along the whole length, which were designed to meet the cyclic loading requirement and achieve the self-balancing. The axial load was applied by a 10,000 kN capacity actuator with the maximum stroke travel of 1000 mm at the end A in Fig. 2(a). The other end B was equipped with the force sensor. The end A was fixed with the concrete strong floor using eight expansion anchoring bolts, while the end B was free to deform in the longitudinal direction but with the constraint in the lateral direction. The test specimen was connected with the two bearings using 20 M36 bolts of grade S8.8 for one end [16]. Two rolling bearings with the lifting jacks were positioned at each 1/3 length to eliminate the deflection of the member causing by its self-weight, as shown in Fig. 2(a).

Figs. 2(b) and 3 show the arrangements of displacement transduces and strain gauges respectively. The strain gauges were mounted on the outer surface of the inner tube at the end B (equipped with the force sensor) and the mid-length to obtain the longitudinal and hoop strains, as shown in Figs. 3(a)-(c). Four linear variable displacement transducers (LVDTs) of a large range and four digital dial-gauge (DDG) were used in the ends A and B respectively to cover the possible maximum displacement during the cyclic test.

2.3 Loading protocol

A quasi-static loading scheme by displacement control was adopted according to AISC 341-16 [17]. On the basis of the Chinese code for seismic design of building (GB 50011-2010) [18], the elastic-plastic storey drift angle requirement for steel structures is 1/50 in seismicity

regions of intensity 6.0-7.0. The relationship between the storey drift angle (θ_s) and the brace deformation (Δ) is calculated by Eq. (1) [19]

$$\Delta_{bm} = \theta_s L \sin \alpha \cos \alpha \quad (1)$$

where, Δ_{bm} is the deformation corresponding the design storey drift; θ_s is the design storey drift; α is the diagonal angle of the bracing member; L is the length of the bracing member.

Therefore, the value of deformation quantity corresponding to the design storey drift (Δ_{bm}) can be calculated using Eq. (1), which is 150 mm for the test specimens in the case of 45° diagonal angle (α). The value of deformation quantity at first significant yield of the specimen (Δ_{by}) can be calculated by Lf_{yi} / E_{si} , where f_{yi} and E_{si} are the yield strength and the Young's modulus obtained from the material coupon tests. Fig. 4 presents the loading protocol for the test. The loading rates for the elastic and plastic loading stages were 0.5 mm/min and 2.5 mm/min, respectively.

3. Finite element model

3.1. General models

Finite element analyses were conducted using ABAQUS/Explicit [20] to replicate the cyclic test of the 15-m SDT-BRB specimen and investigate the hysteretic response and behaviour deterioration evolution during the loading cycles. The full test specimen was modelled because the contact between inner and outer tubes might occur non-symmetrically. To further validate the developed FE model, the plain steel trible-tube BRB test results from Zhang *et al.* [9] was also employed.

The three-dimensional solid elements, C3D8Rs, were adopted in FE models in order to capture the fracture phenomenon precisely, i.e. the processes of fracture initiation on the surface and the propagation throughout the thickness. The hard contact model and Coulomb friction model were employed in the normal and tangential directions, respectively, to

simulate contact behaviour between the inner (slave surface) and outer (master surface) tubes. All degrees of freedom on the end surfaces were coupled to the rigid plates. The axial loading and the boundary conditions were applied to the reference points on the ridged plates, RP1 and RP2, respectively, which were in accordance with the test condition, as described in Fig. 5.

The geometric imperfection was incorporated in the FE models by defining the global imperfection based on the superposition of weighting eigenmode analysis data. The effects of different imperfection values on the performance of the double-tube bracing members were analysed. The results and discussions are presented in the following section. The amplitude of global and local imperfections are 1.5 mm and 0.1 mm, respectively.

The typical mesh of the FE model is shown in Fig. 5. In the thickness direction, the element number was set to be three for both inner and outer tubes, while on the circumference, the element numbers of the inner and outer tubes were 88 and 36 respectively. The refined mesh was used in the inner tube, where the regions were near the end of the stiffeners, as marked in Fig. 5. Therefore, in the case of the outer tube, the element size (longitudinal direction $\times$ circumferential direction) was 100 mm $\times$ 50 mm; while in the case of inner tube, it was 50 mm $\times$ 20 mm for the non-refined region and was 20 mm $\times$ 20 mm for the refined region. The total element number was approximate 130,600.

In the explicit analysis technique, despite the convergence problem will not occur, the computational efficiency and accuracy should be particularly considered. Essentially, to achieve the accurate simulation results using the explicit calculation method, the stable increment should be small enough. However, this can lead to massive computational time in solving the quasi-static problems because of the relative long loading duration compared with some dynamic events, such as impact and explosion simulation. There are two methods to reduce the total computational time for the quasi-static simulation when using ABAQUS/Explicit, which are described as followings:

1) Speed up the load rate in the test to reduce the total loading time (by reducing the total time period for the displacement loading method or increasing the loading rate in the velocity loading method);

2) Present the mass scaling factor (MSF) to artificially increase the stable increment time.

In this study, MSF was employed to reasonably reduce the computational time. It should be noted that these two methods might inevitably increase the inertial forces which significantly alters the solution when an inappropriate value is adopted. Therefore, the sensitive study was conducted to achieve the balance between the computational accuracy and efficiency. The ratio of all kinetic energy (ALLIK) to all internal energy (ALLIE) was used to assess the feasibility of the adopted MSF values in the FE analysis [21,22], since ALLIK/ALLIE indicated the stability of the simulation procedure. The load-displacement hysteretic curves from the FE models with the MSF values of 10^6 , 10^7 , 10^8 , and 10^9 are compared in Fig. 6(a). The largest MSF value, i.e. 10^9 , results the significantly unstable response during the initial loading steps. Moreover, the early fracture was predicted as the MSF value increased, owing to the artificial dynamic effects caused by the inappropriate computational acceleration. Meanwhile, in the perspective of the dynamic effects, the ALLIK-to-ALLIE ratios of the 10^6 -, 10^7 - and 10^8 -MSF models were compared in Fig. 6(b). The ratios for 10^6 -, 10^7 - MSF models are controlled under a low level during the entire loading period. However, the model of 10^6 MSF took more than three times computational time (up to 7 days) than the one with 10^7 MSF (approximately 42 hours). Therefore, the MSF value of 10^7 was adopted in this study after optimizing the computational accuracy and efficiency.

3.2. Plasticity model

The mechanical behaviour of steel under cyclic loading was simulated by adopting the constitutive model with von-Mises yield criterion and the combined isotropic/kinematic

hardening rule in ABAQUS [21]. The Young's modulus and the Poisson's ratio were 207 GPa and 0.3 respectively. The isotropic hardening component is expressed in Eq. (2).

$$\sigma_0 = \sigma|_0 + Q_\infty \left(1 - e^{-b \bar{\varepsilon}^{pl}}\right) \quad (2)$$

where $\sigma|_0$ is the yield stress at zero plastic strain, Q_∞ is the maximum change in size of yield surface, b is the rate of change of yield surface size with increasing plastic strain, $\bar{\varepsilon}^{pl}$ is the equivalent plastic strain using Eq. (3)

$$\bar{\varepsilon}^{pl} = \int_0^t \sqrt{\frac{2}{3}} d\varepsilon^{pl} : d\varepsilon^{pl} dt \quad (3)$$

The nonlinear kinematic hardening component using the backstress, α , was described in Eqs. (4) and (5)

$$d\alpha_k = C_k \frac{1}{\sigma_0} (\sigma - \alpha) \cdot d\bar{\varepsilon}^{pl} - \gamma_k \alpha_k \cdot d\bar{\varepsilon}^{pl} \quad (4)$$

$$\alpha = \sum_{k=1}^n \alpha_k \quad (5)$$

where σ_0 is the size of the yield surface defined in Eq. (2), C_k is the k^{th} initial kinematic hardening moduli, γ_k is k^{th} rate of decrease of C_k , α is the overall backstress; α_k is the k^{th} back stress;

Table 2 presents the constitutive model parameters calibrated according to ABAQUS documentation [21] from material test.

3.2. Fracture model

The phenomenological ductile fracture model, Modified Mohr-Coulomb (MMC) model [23] has been adopted in this study. This fracture model will capture the effects of stress triaxiality (η) and Lode angle (θ) on the fracture initiation, which has been extensively

verified in various metal material tests [23-25]. Meanwhile the ductile fracture of structural components under monotonic loading has been successfully predicted using MMC prediction model with a linear damage accumulation rule [22-23,26-27]. In the current study, MMC model with a nonlinear damage evolution [24,25] was adopted to the reversed or cyclic loading conditions.

3.2.1 Definition of stress state variables

A spherical coordinate system defined stress triaxiality (η), Mises stress ($\bar{\sigma}$), and Lode angle (θ) was proposed by Bai and Wierzbicki [28] as a new stress space. It can be transformed from the stress space in Cartesian coordinate system. The relations are given by Eqs. (6) - (13).

The first invariant of the stress tensor is defined by Eq. (6)

$$I_1 = \sigma_1 + \sigma_2 + \sigma_3 \quad (6)$$

The second and third invariants of the deviatoric stress tensor are expressed in Eqs. (7) and (8) respectively.

$$J_2 = \frac{1}{2} s_{ij} s_{ij} \quad (7)$$

$$J_3 = \frac{1}{3} s_{ij} s_{jk} s_{ki} \quad (8)$$

The deviatoric stress tensor is described as Eq. (9)

$$s_{ij} = \begin{bmatrix} \sigma_1 - \sigma_m & 0 & 0 \\ 0 & \sigma_2 - \sigma_m & 0 \\ 0 & 0 & \sigma_3 - \sigma_m \end{bmatrix} \quad (9)$$

where $\sigma_1, \sigma_2, \sigma_3$ are the principle normal stresses; σ_m in the mean value of normal stress $\frac{1}{3} I$.

The stress triaxiality and Lode angle are defined in Eqs. (10) and (11) respectively

$$\eta = \frac{\sigma_m}{\sigma} \quad (10)$$

$$\theta = \frac{1}{3} \arccos \left(\frac{27}{2} \frac{J_3}{\bar{\sigma}^3} \right) \quad (11)$$

$$\bar{\sigma} = \sqrt{3J_2} \quad (12)$$

where $\bar{\sigma}$ is Mises stress, J_2 and J_3 are the second and third deviatoric stress invariants respectively.

The normalized Lode angle, $\bar{\theta}$, is adopted in the MMC fracture prediction model [23] which is employed in the FE analysis in this study. The normalized Lode angle is presented as Eq. (13).

$$\bar{\theta} = 1 - \frac{2}{\pi} \arccos \left(\frac{27}{2} \frac{J_3}{\bar{\sigma}^3} \right) \quad (13)$$

3.2.2 Phenomenological fracture model

To efficiently detect the initiation of ductile fracture, the Mohr-Coulomb criterion has been transferred from a stress-based model to a strain-based model in a space of $(\bar{\varepsilon}^{pl}, \eta, \bar{\theta})$ by Bai and Wierzbicki [28]. When a von Mises yield surface were adopted, the MMC fracture model is expressed in Eq. (14) [23].

$$\bar{\varepsilon}_f = \left\{ \frac{A}{c_2} \left[\sqrt{\frac{1+c_1^2}{3}} \cos \left(\frac{\bar{\theta}\pi}{6} \right) + c_1 \left(\eta + \frac{1}{3} \sin \left(\frac{\bar{\theta}\pi}{6} \right) \right) \right] \right\}^{-\frac{1}{n}} \quad (14)$$

where $\bar{\varepsilon}^{pl}$ is the equivalent fracture strain, calculated using Eq. (3); $\bar{\theta}$ is the normalized Lode angle, in Eq. (13); η is the stress triaxiality, in Eq. (10); A and n , are the hardening exponent and the amplitude respectively, which can be calibrated from the true stress-strain curve by fitting Hollomon's [29] or Swift's [30] power laws; c_1 and c_2 are the friction coefficient and the maximum shear strength in Mohr-Coulomb model, respectively.

A cut-off region in the negative stress triaxiality range, where fracture strain will go to infinity and therefore ductile fracture cannot occur, is defined in Eq. (15) [23].

$$\sqrt{\frac{1+c_1^2}{3}} \cos\left(\frac{\bar{\theta}\pi}{6}\right) + c_1 \left(\eta + \frac{1}{3} \sin\left(\frac{\bar{\theta}\pi}{6}\right) \right) \leq 0 \quad (15)$$

To calibrate the parameters, a comprehensive material test programme should be performed, which is designed to cover the various stress triaxiality regimes at the onset of fracture initiation using specially designed coupons. It is also required that the measurement can obtain the true stress-strain evolution and recognise the onset of fracture initiation during the whole loading history. This is a very complicated test and post-processing procedure. To facilitate an effortless application of MMC model in structural analyses, a simplified calibration procedure, based on the conventional tensile tests, for MMC model was proposed and verified in previous studies [22, 26-27]. More detailed information for the simplified calibration procedure can be found in Xu et al. [22, 26]. The calibrated parameters in this study are listed in Table 3.

3.2.3 Nonlinear damage evolution rule

The linear damage accumulation rule was found to accurately predict material fracture initiation in the cases of monotonic loading [23, 28]. However, the nonlinear damage evolution itself and the load history effect should be considered in the cases of reversed loading [24-25,31-32]. Therefore, the MMC model was extended to the reversed loading by adopting a non-linear damage evolution rule [24]. Eqs. (16) and (17) account for the non-linear damage accumulation and the load reversal effect respectively. The parameters value of c_g , c_h , β_1 , β_2 and k_h are presented in Table 3.

$$F_D(D_F) = \left(c_g D_F + \frac{c_g}{e^{c_g} - 1} \right) \quad (16)$$

$$F_H(D_F, \mu_h) = \left(1 + c_h \cdot D_F^{\beta_1} \cdot \mu_h^{\beta_2} \right)^{k_h} \quad (17)$$

where c_g is the parameter of the extension function for nonlinear damage rule; c_h, β_1, β_2 and k_h are the parameters of the extension function for load-change effects; μ_h is in terms of the backstress and current stress tensors to capture the reverse loading effects given by Eq. (18)

$$\mu_h = \int_0^{\bar{\varepsilon}^{pl}} \left(1 - \frac{\sigma_{ij} : \alpha_{ij}}{\|\sigma_{ij}\| \cdot \|\alpha_{ij}\|} \right) d\bar{\varepsilon}^{pl} \quad (18)$$

where α and σ are the backstress and current stress tensor respectively; $\bar{\varepsilon}^{pl}$ is equivalent plastic strain.

The incremental form of damage accumulation law is shown in Eq. (19). It indicated that ductile fracture initiates at the material point when D_{FI} is equal to unity.

$$dD_{FI} = F_D \cdot F_H \frac{d\bar{\varepsilon}^{pl}}{\varepsilon_f(\eta, \theta)} \quad (19)$$

3.2.4 Post-initiation softening

When D_{FI} reaches 1.0, a post-initiation softening rule is used to quantify the material deterioration in a linear form between the material degradation and the equivalent plastic strain. In the FE models, the effective plastic displacement was adopted to avoid significant dependence of the mesh [21]. The post-initiation softening rule is expressed in Eqs. (20) and (21). It shows that the material point fails (deleting element in the Explicit solving technique) when D_{PI} is equal to unity.

$$E' = (1 - D_{PI}) \cdot E \quad \text{and} \quad \bar{\sigma}' = (1 - D_{PI}) \cdot \bar{\sigma} \quad (20)$$

$$dD_{PI} = \frac{L_c}{\bar{u}_f^{pl}} \cdot d\bar{\varepsilon}^{pl} \quad (21)$$

where D_{PI} is the post-initiation degradation indicator, L_c is the characteristic length of the element and $\bar{u}_f^{pl}$ is the effective plastic displacement.

The MMC model (Eq. (14)) with the nonlinear damage accumulation rule (Eqs. (16) - (19)) was incorporated into the finite element analysis using user subroutine, VUSDFLD. The

element deletion provided in ABAQUS/Explicit is activated when D_{PI} reaches 1.0. This can simulate the visual fracture cracking.

4. Test results and FE model validation

4.1. Test results

The failure mode of the 15 m test specimen is shown in Figs. 7(a) and (b). The inner tube of the test specimen fractured at the tips of the inner stiffeners after several inelastic loading cycles. The straight seam welds of inner and outer tubes were visually undamaged after the test. No global buckling occurred during the test. Local buckling was only observed at the fracture region. The premature fracture can be attributed to the accumulated localized plastic strain which is mainly induced by the stress concentration at the tips of the stiffeners. Therefore, to achieve the satisfactory performance of the stiffened double-tube bracing members, the influence of the dimensions and arrangements of the stiffeners were investigated in the parametric study.

The load-displacement curve of test specimen is shown in Fig. 8. The fracture onset is indicated in the curve during the compressive loading excursion. The behaviour of inner tube was monitored by the strain gauges since it was unable to be observed visually during test. The arrangement and records of the strain gauges are shown in Figs. 3 and 9 respectively. Fig. 9(a) presents the average longitudinal strain evolution at the end and mid-sections, where SGE and SGM refer the strain gauges mounted on the end and mid-section. The end-stiffeners have minor effects on the initial stiffness, while the discrepancies of the stiffness are found between the end and mid-sections owing to additional stiffeners. The longitudinal strain readings of SGEs 1-3 on the end section are presented in Fig. 9(b). It indicates the uniformly distributed elastic stresses of the un-stiffened region of the inner tube beyond the outer tube, where it is designed not to be yielded or buckled. Fig. 9(c) shows the average hoop strain evolutions at the end and mid-sections. The stable and saturated hoop strain

hysteretic curves are developed at the mid-section, demonstrating that no global and local buckling occurred at the mid-length.

The characteristics of hysteretic curve of the test specimen, such as adjustment factors for the compressive-capacity and strain hardening, hysteretic shape and energy dissipated are presented in Tables 4 - 5 and discussed in the following section.

4.2. Validation of FE model

The comparison of the force-displacement curves between the experimental and numerical results is shown in Fig. 8. The fracture processes of the FE result, i.e. initiation and cracking throughout the thickness, are presented. As anticipated, premature fracture initiated at the end of the inner stiffeners where the sudden change of the local stiffness existed. This sudden change of stiffness resulted in the stress concentration and local buckling during the periodic loading history, as shown in Figs. 7(b) and (c). To further validate the established FE models in capturing the fracture onset of plain steel BRBs, the test results of tri-tube BRB specimens from Zhang, *et. al* [9] was adopted for comparisons. The specimens which failed at fracture were replicated in the numerical simulation. The calibrated parameters for the steel plasticity and fracture models are listed in Tables 2 and 3. The force-displacement curves with fracture initiation marked are compared in Fig. 10. Furthermore, the hysteretic curves calculated from the developed FE models are also compared with those calculated from non-fracture associated models, as shown in Figs. 11(a) and (b). The FE models without fracture criterion would go further and complete the whole specified loading protocols without any occurrence of failure, i.e. local buckling, global buckling and deteriorations of the strength and stiffness. These non-fracture associated FE models would overestimate the ductility capacity and dissipated energy, leading to inaccurate cyclic structural behaviour. The comparisons also demonstrate the importance and necessity of incorporating the ELCF fracture model in the numerical simulation of BRB.

The failure modes of test specimens in the present and previous studies are compared in Figs. 7 and 10 respectively. The FE models are capable of predicting the fracture initiation. Moreover, the comparison in Fig. 7(b) also demonstrates the FE model can capture the buckling-induced fracture and stress-concentration-induced fracture. The equivalent plastic strain distribution at the onset of fracture initiation is presented in Fig. 7(c). It indicates that the fracture initiates from the inner surface of the inner tube at the critical positions where the significant stress concentration exists.

The evolution of accumulation damage, D_{FI} , versus stress triaxiality and normalized Lode angle of the first fracturing element (in Fig. 7(b)) as marked is shown in Fig. 12. During the loading history, the stress triaxiality and normalized Lode angle approximately of the element ranged from -0.6 to 0.6 and -0.8 to 0.8 respectively. This also indicated that the element was under multi-axial stress states. In addition, the equivalent plastic strain increased significantly at the last cycle to fracture initiation.

Generally, the comparison of test and numerical results for two test programmes shows the applicability of the adopted fracture model in the member-level ELCF fracture simulation. It also demonstrates that the newly developed FE models can accurately simulate the hysteretic response and fracture occurrence of the stiffened double-tube BRB under ELCF.

5. Parametric studies

The parametric studies were conducted to further assess the influence of the inner stiffeners, material property, gap size and imperfection on the cyclic behaviour of the double-tube BRBs. The test specimen was considered as the benchmarked specimen, labelled as OT550-25-IT480-14-GP10-L15, where OT, IT, GP and L refer to the outer tube, inner tube, gap and length respectively; the numbers following OT and IT represent the tube diameter (i.e. 550 mm for outer tube and 480 mm for the inner tube) and thickness of the corresponding tubes

(i.e. 25 mm for the outer tube and 14 mm for the inner tube); the numbers following GP and L are the gap size (i.e. 10 mm) and member length (i.e. 15 m).

5.1 Effect of end-stiffeners

As observed in the test, the fracture initiated from the inside surface of the inner tube at the stiffener tips(see Fig. 7(c)). Therefore, the different arrangements of the stiffeners are investigated using the established FE models, with the purpose to mitigate the stress concentration level. Three cross-sections are proposed for the comparison, i.e. eight-stiffener (ES) section, four-stiffener (FS) section and changeable-thickness (CT) section. The dimensions of the stiffeners and tube-wall thickness at the end are designed based on the criterion that the stiffened region remains elastic without local buckling during the entire loading history. The proposed sections are shown in Fig. 13(a). The hysteretic responses of three kinds of sections are presented in Fig. 13(b). It demonstrates that different stiffener arrangements have minor influence on the hysteretic curves; however, significant discrepancies are found on the onset of fracture. For the CT section series, the stress concentration is mitigated because of the smooth transition zone between the stiffened and effective regions. For the FS section series, the longer distance between two hot-spots of stress concentration can also mitigate the whole section deterioration compared with the ES section.

5.2 Effect of material property

Three different materials for the inner tube, i.e. the low-yield-point steel (LYP225 [33]), the mild steel (Q235 steel) and the stainless steel (EN 1.4301 [34]), are employed to evaluate the influences of different material properties. The parameters of plasticity and fracture models are calibrated from the material tests [33-34] as presented in Tables 2 and 3. The material property of the outer tube is keep the same as the test specimen. The force-displacement

curves from the FE analyses are normalised by their yield strengths to facilitate the comparison between the materials having different yield strengths. Fig. 13(a) shows that in the case of ES section models (in the same dimension as the test specimen) the low yield point steel can achieve most saturated hysteretic loops and reach the 1% deformation. However, owing to the dominant influence of the stress concentration effect caused by the end-stiffeners, the FE models with these three materials fail at the same cycle. Fig. 13(b) presents that in the case of changeable-thickness section models, OT550-25-IT480-14-GP10-L15-CT, the FE models with LYP 225 and EN 1.4301 fractured at the second cycle of $1/75L$ deformation, which exhibit better ductility than the model with Q235 material. Furthermore, as seen in Figs. 13(a) and (b), for the FE models with material EN 1.4301, both tensile and compressive strengths cannot reach the nominal yield strength until 1% deformation. The comparisons of adjustment factors for the compressive-capacity, strain hardening, hysteretic shape and energy dissipated between each material are discussed in the following section.

5.3 Effect of gap and global imperfections

The gap size between the outer and inner tubes is a primary parameter for the design, fabrication and installation. The clearance is essential for the transfer of the lateral force from the inner tube to the outer tube while preventing the load transfer in the longitudinal direction. The small clearance would cause increasing difficulties in the fabrication and installation processes; in the case of the larger clearance, the fluctuations in the hysteretic curves were reported [8]. Therefore, the influence of the gap size on the hysteretic response are evaluated for the FE models with different end-stiffener arrangements. The geometry properties of the FE models with different gap values are listed in Table 6. The member length and dimensions of the inner tubes keep constant, while the diameter and thickness of the outer tube change in accordance with the gap size to keep an identical confinement ratio

($F_{\text{cri}}/f_{\text{si}}A_{\text{si}}$) for all parametric models, where F_{cri} is the Euler critical force calculated using Eq. (22).

$$F_{\text{cri}} = \frac{\pi^2 E_{\text{so}} I_{\text{so}}}{(KL)^2} \quad (22)$$

where E_{so} is the Young's modulus of the outer tube, I_{so} is the moment of inertia of the cross section of the outer tube, K is the column effective length factor and L is the member length.

The results from the FE analyses are presented in Fig. 14. The large gap size, i.e. 20 mm, has effects on the hysteretic response where fluctuations are observed. There is negligible influence on the stable and saturated properties of the hysteretic curves for the 5 mm and 10 mm gap series, as shown in Figs. 14(a) and (b). Since the stress concentration is the main failure mode for the ES series, the influence of the gap size on the fracture initiation is less important. In the case of the CT series, the larger gap leads to the earlier fracture initiation especially for the model with a 20 mm gap as shown in Fig. 14(b).

The initial global imperfection of the inner tube is unavoidable during the manufacturing and installation processes of a full-scale BRB. Therefore, the hysteretic response and the fracture initiation for the FE models with the initial global imperfection values ranging from $L/10000$ to $L/2000$ are compared in Fig. 15. The imperfection influence on BRB models with different end sections, i.e. eight-stiffener and changeable-thickness sections, are investigated and the force-deformation curves and fracture onsets are compared, as shown in Figs. 15. It should be noted that, the imperfection value of $L/2000$ (7.5mm) for the 5 mm gap series is larger than the gap size and is excluded in the parametric study. Figs 15(a) and (b) demonstrate that the global imperfection has negligible influence on the hysteretic response and the fracture initiation.

Therefore, the gap size should be limited to 20 mm to achieve the stable hysteretic response. The gap size ranging from 5 mm to 10 mm is suggested considering the fabrication and installation based on the parametric study.

5.4 Adjustment factors, hysteretic curve shape factors and dissipated energy

The compressive-strength adjustment factor, β , accounting for the tension-compression symmetry property and the tension strain hardening adjustment factor, ω , are calculated according to AISC 341-16 [17] using Eqs. (23) and (24).

$$\beta = \frac{F_{c,\max}}{F_{t,\max}} \quad (23)$$

$$\omega = \frac{F_{t,\max}}{f_{ys} A_{si}} \quad (24)$$

where $F_{c,\max}$ and $F_{t,\max}$ are respectively the maximum compressive and tensile forces; f_{ys} is the yield strength of inner tube; A_{si} is the section area of inner tube.

The calculated adjustment factors, i.e. β and ω , for the FE models with various material properties and stiffener arrangements aforementioned are listed in Table 4. At the cycle prior to fracture, the values of β range from 1.01 to 1.08, which satisfies the requirement in AISC seismic provisions [17] (i.e. $\beta \leq 1.3$). The values of ω range from 0.91 to 1.32, and the ES model in EN 1.4301 fail to reach the yield strength. Despite stainless steel can possess large elongation before fracture (more than 50% [34]), the performance of bracing members benefits little from this merit.

The shape factor, χ , is adopted to assess the pinching effect of the hysteretic curves as defined schematically in Fig. 16 [13]. The average values of χ at each loading amplitude are calculated and presented in Table 5. The shape factor χ gradually increases as the amplitude, indicating that the hysteresis loops can be more saturated at a larger amplitude step. The FE models with LYP225 achieve the highest χ values among the compared materials at a same loading amplitude; while the models with EN 1.4301 fail to reach 3.0 until the amplitude of $1/75L$. In addition, according the Chinese seismic design provision (GB5001-2010) [18], the

equivalent ratio of critical viscous damping (ξ_{eq}) of BRBs is defined in Eq. (25) which is $\frac{1}{2\pi}$ time of the shape factor χ . The SDT-BRBs could achieve a high value (> 0.3) and the maximum value is obtained by the FE model OT550-25-IT480-14-GP10-L15-CT with LYP225.

$$\xi_{eq} = \frac{S_{FBC} + S_{EBC}}{2\pi(S_{\Delta ODF} + S_{\Delta OAE})} \quad (25)$$

The values of the total hysteretic energy dissipated for each model are compared in Table 4. The normalized value is adopted to facilitate the comparison among models with different material properties. The CT model with LYP225 achieves the highest normalized value, and the ES model with EN 1.4301 behaves least favourably owing to the deficient ductility, low adjustment factor and small shape factor.

6. Conclusions

A stiffened double-tube buckling-restrained brace was proposed for boiler steel plant structures. The full-scale experiment was conducted on the 15 m bracing member to investigate the performance of the proposed BRB in the practical structures. A finite element model was established with material fracture criterion to further evaluate the hysteretic behaviour and to accurately predict the fracture failure. The parametric study was carried out on some practical concerning issues, i.e. the effects of the stiffener arrangement, material property, gap size and imperfection. Major findings obtained from this study are as follows:

- 1) The hysteretic loops prior to failure was stable and saturated. No negative effects of the manufacturing processes of cold-form and straight welding on the hysteretic response of the large-scale BRB, i.e. a 15 m test specimen. The premature fracture initiated at the inside surface of the inner tube at the stiffener tips where the severe

stress concentration existed, leading to failure of the test specimen. No global buckling occurred during the loading cycles.

2) The FE models incorporating a material fracture criterion and a damage evolution law were developed and validated by the test results from different experimental programmes. The FE models without a material fracture criterion would probably overestimate the ductility capacity of BRBs. The comparison of different FE models also manifested the accuracy and reliability of the model in predicting the behaviour deterioration and failure onset.

3) Based on the parametric study, both methods of reducing the stress concentration level and increasing the hot-spot distance would mitigate the risk of premature fracture. The Q235 and LYP225 steels were both found to be appropriate for the inner tube. The gap size between the inner and outer tubes should be controlled to achieve a stable hysteretic behaviour.

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599 **List of Figure Captions**

600 **Figure 1.** Test specimen

601 **Figure 2.** Test setup

602 **Figure 3.** Location of the strain gauges

603 **Figure 4.** Loading protocol

604 **Figure 5.** Finite element model

605 **Figure 6.** Comparison of hysteretic responses from test and simulation

606 **Figure 7.** Failure mode of the test specimen and FE model

607 (Eight times magnified deformations at the cross-section)

608 **Figure 8.** Comparison of hysteretic responses from test and simulation

609 **Figure 9.** Average values of measured strains

610 **Figure 10.** Comparison of hysteretic responses from test and simulation [9]

611 **Figure 11.** Comparison of hysteretic responses from different FE models

612 **Figure 12.** Relationship of fracture index with (a) stress triaxiality (b) normalized Lode angle

613 **Figure 13.** Effects of the stiffener arrangements

614 **Figure 14.** Effects of material properties

615 **Figure 15.** Effects of gap size

616 **Figure 16.** Effects of global imperfections

617 **Figure 17.** Schematic view for the definition of χ

618 **List of Table Captions**

619 **Table 1.** Measured material properties

620 **Table 2.** Mechanical properties and parameters of constitutive model for materials

621 **Table 3.** Parameters of the fracture model and damage rule used in FE models

622 **Table 4.** Summary of strain hardening and compressive strength adjustment factors

623 **Table 5.** Summary of χ and ξ_{eq} at each amplitude

624 **Table 6.** Dimensions for the parametric FE models with different gap sizes

Table 1. Measured material properties

Regions	f_y (N/mm ²)	f_u (N/mm ²)	E (kN/mm ²)
Stiffener	259.3	390.6	198.3
Outer tube	260.3	384.0	202.7
Inner tube	255.3	383.7	207.0

Table 2. Mechanical properties and parameters of constitutive model for materials

Material types	E_s (GPa)	f_y (MPa)	f_u (MPa)	$\sigma _0$ (MPa)	Q_∞ (MPa)	b	C_k (MPa)	γ_k
Q235	207.0	255.3	383.7	232.3	21	1.2	55189	986.0
							5000	55.0
							3000	84.0
LYP225 [33]	195.1	221.0	327.0	165.0	53	5.0	62105	1611.0
							4986	413.0
							1978	95.0
							701	3.0
EN 1.4301 [34]	182.5	417.3	702.5	184.3	21	1.2	39731	698.0
							39284	248.0
							2429	2.3
Q235 [9]	206.0	422	639	422	--	--	9000	250.0
							2000	150.0
							4000	100.0

Table 3. Parameters of the fracture model and damage rule used in FE models

Material types	A (MPa)	n	c_1	c_2 (MPa)	c_g	c_h	β_1	β_2	k_h
Q235	623.6	0.182	0.12	287.8	-6	5.5	2	2	0.6
LYP225 [33]	570.9	0.232	0.12	245.3	-6	5.5	2	2	0.6
EN 1.4301 [34]	1361.5	0.313	0.12	526.9	-6	5.5	2	2	0.6
Q235 [9]	745.4	0.209	0.12	377.8	-6	5.5	2	2	0.6

Table 4. Summary of strain hardening and compressive strength adjustment factors

	Specimens	Remarks	Material types	Number of cycles to failure	β		ω		Total normalized hysteretic energy absorption $E_{\text{total}} / (f_{yi} A_{si} L)$
					Before fracture	1st cycle at 1/100L	Before fracture	1st cycle at 1/100L	
Test	OT550-25-IT480-14-GP10-L15	Test specimens	Q235	1st cycle at 1/100L	1.01	--	0.03	--	0.03
	OT550-25-IT480-14-GP10-L15-ES		Q235	1st cycle at 1/100L	1.02	--	0.04	--	0.04
	OT550-25-IT480-14-GP10-L15-ES	with eight stiffeners (ES) at both ends	LYP225 [33]	1st cycle at 1/100L	1.03	--	0.04	--	0.04
	OT550-25-IT480-14-GP10-L15-ES		EN 1.4301 [34]	1st cycle at 1/100L	1.01	--	0.02	--	0.02
Para. study	OT550-25-IT480-14-GP10-L15-FS	with four stiffeners (FS) at both ends	Q235	1st cycle at 1/75L	1.04	1.07	0.12	1.29	0.12
	OT550-25-IT480-14-GP10-L15-CT		Q235	1st cycle at 1/75L	1.05	1.02	0.12	1.29	0.12
	OT550-25-IT480-14-GP10-L15-CT	with gradually changed thickness (CT) at both ends	LYP225 [33]	2nd cycle at 1/75L	1.08	1.07	0.18	1.12	0.18
	OT550-25-IT480-14-GP10-L15-CT		EN 1.4301 [34]	2nd cycle at 1/75L	1.06	1.05	0.13	0.99	0.13

Table 5. Summary of χ and ξ_{eq} at each amplitude

	Specimens	Remarks	Material types	Number of cycles to failure	χ (defined in Fig. 17)			ξ_{eq} (defined in Eq. 25)		
					1/200L	1/100L	1/75L	1/200L	1/100L	1/75L
Test	OT550-25-IT480-14-GP10-L15	Test specimens	Q235	1st cycle at 1/100L	3.34	--	--	0.53	--	--
	OT550-25-IT480-14-GP10-L15-ES		Q235	1st cycle at 1/100L	3.14	--	--	0.50	--	--
	OT550-25-IT480-14-GP10-L15-ES	With eight stiffeners (ES) at both ends	LYP225 [33]	1st cycle at 1/100L	3.43	--	--	0.55	--	--
	OT550-25-IT480-14-GP10-L15-ES		EN 1.4301 [34]	1st cycle at 1/100L	2.30	--	--	0.37	--	--
Para. study	OT550-25-IT480-14-GP10-L15-FS	with four stiffeners (FS) at both ends	Q235	1st cycle at 1/75L	3.08	3.23	--	0.49	0.51	--
	OT550-25-IT480-14-GP10-L15-CT		Q235	1st cycle at 1/75L	3.11	3.22	--	0.49	0.51	--
	OT550-25-IT480-14-GP10-L15-CT	with gradually changed thickness (CT) at both ends	LYP225 [33]	2nd cycle at 1/75L	3.25	3.42	3.42	0.52	0.54	0.54
	OT550-25-IT480-14-GP10-L15-CT		EN 1.4301 [34]	2nd cycle at 1/75L	2.47	2.89	3.03	0.39	0.46	0.48

Table 6. Dimensions for the parametric FE models with different gap sizes

Specimens	Remarks	Gap size (mm)	Outer tube		Inner tube	
			Diameter (mm)	Thickness (mm)	Diameter (mm)	Thickness (mm)
OT542.6-26.3- IT480-14-GP5- L15-ES		5	542.6	26.3	480.0	14.0
OT550-25- IT480-14- GP10-L15-ES	with eight stiffeners (SS) at both ends	10	550.0	25.0	480.0	14.0
OT565.4-22.7- IT480-14- GP15-L15-ES		20	565.4	22.7	480.0	14.0
OT542.6-26.3- IT480-14-GP5- L15-CT		5	542.6	26.3	480.0	14.0
OT550-25- IT480-14- GP10-L15-CT	with gradually changed thickness (CT) at both ends	10	550.0	25.0	480.0	14.0
OT565.4-22.7- IT480-14- GP15-L15-CT		20	565.4	22.7	480.0	14.0

(a) Schematic view

(b) Photos

Figure 1. Test specimen

(a) Set-up

(b) Displacement measurement

Figure 2. Test setup

Figure 3. Location of the strain gauges

Figure 4. Loading protocol

Figure 5. Finite element model

(a) Effect of MSFs

(b) ALLKE/ALLIE

Figure 6. Comparison of hysteretic responses from test and simulation

(a) Global view

(b) Local view

(c) Equivalent plastic strain at fracture initiation

Figure 7. Failure mode of the test specimen and FE model
(Eight times magnified deformations at the cross-section)

Figure 8. Comparison of hysteretic responses from test and simulation

(a)

(b)

(c)

Figure 9. Average values of measured strains

Figure 10. Comparison of hysteretic responses from test and simulation [9]

Figure 11. Comparison of hysteretic responses from different FE models

(a) Stress triaxiality

(b) Normalized Lode angle

Figure 12. Relationship of fracture index with (a) stress triaxiality (b) normalized Lode angle

(a) Schematic view of different end sections

(b) Hysteretic curves

Figure 13. Effects of the stiffener arrangements

Figure 14. Effects of material properties

(a) Eight-stiffener section

(b) Changeable thickness section

Figure 15. Effects of gap size

(a) Eight-stiffener section

(b) Changeable thickness section

Figure 16. Effects of global imperfections

Figure 17. Schematic view for the definition of χ