INELASTIC CYCLIC BEHAVIOR AND FRACTURE OF WIDE FLANGE STEEL BRACE MEMBERS

Madhar HADDAD¹, Rami HADDAD², Arabi AL QADI³, Hashem AL-MATTAReNHz⁴

ABSTRACT

Wide flange (WF) steel brace members are increasingly used in framed structures to resist seismic excitations. In the current study, a finite element model with a refined fracture model is presented. Nonlinear elasto-plastic finite element model is used to simulate the behavior of selected WF steel members connected to gusset plates at their ends when subjected to reversed axial displacements. A combined isotropic-kinematic hardening material model is used. Four-node finite membrane strain quadrilateral shell elements are used to model the WF brace members. Fixed boundary conditions are applied at the ends of the WF braces with axial displacements imposed for each cycle. The model is able to simulate the hysteresis behavior and to predict fracture life of the WF brace members. The effect of number of integration points, mesh density and thickness of gusset plate on the hysteresis behavior and fracture life is investigated. The model is able to simulate the shift in location of the mid-length plastic hinge. True stress-strain curves are used in modeling the WF brace members. It is recommended to reduce the tangent modulus of the engineering stress-strain curve before converting it to true stress-strain curve to account for accumulated increase in heat that can promote the movement of dislocations during cyclic loading.

Keywords: Wide flange steel brace members; Design; Finite element analysis; Fracture

1. INTRODUCTION

Wide flange (WF) steel sections are increasingly used as brace structural members in the construction of framed structures as shown in figure 1. WF bracing members are often arranged in opposing pairs when used for medium and high-rise buildings in seismically active regions. The availability of WF sections that meets the stringent compactness limits is more than what is available for tubular sections. These WF sections have greater available compressive resistance than tubular sections and therefore will be selected as brace members in high seismic region. In addition, WF brace members can achieve greater ductility compared with their tubular counterparts (Popov and Black (1981), Gugerli and Goel (1982), Fell et al. (2006), Tremblay et al. (2008), Richard (2009), Powell (2009), Clark (2010), Hsiao (2010), Lai (2013)). Thus, WF brace members can provide a more desirable seismic-resistant braced frame response than tubular bracing members. In WF sections, local buckling occurs more gradually and thereby delays fracture initiation at the mid-length plastic hinge of the brace. The longer length of the plastic hinge at the mid-length of the WF braces compared to the corresponding length of the plastic hinge at the mid-length of tubular braces is due to the unrestrained local buckling and low torsional resistance of WF braces compared to tubular braces.

Design, analysis and even behavior of WF brace members are still concerns despite recent increase in experiments and advancement in finite element analysis. Finite element models (Haddad et al. (2004, 2006, 2011, 2015, 2017), Uriz et al. (2005, 2008), Fell et al. (2006, 2010), Myers et al. (2009), Huang and Mahin (2008, 2010), Lai (2013)) are able to simulate the phenomena of local buckling which can lead to fracture of brace members. The current paper describes a finite element model with a refined

¹Dr. Madhar Haddad, AE Department, Assistant Prof., Al Ain, UAE University, madhar@uaeu.ac.ae
²Dr. Rami Haddad, CE Department, Prof., Irbid, Jordan, rhaddad@just.edu.jo
³Dr. Arabi Al Qadi, CE Department, Assistant Prof., Ajloun, Jordan, arabi.alqadi@gmail.com
⁴Dr. Hashem Al-Mattarneh, CE Department, Associate Prof., Najran, Saudi Arabia, drhashem2010@yahoo.com
fracture rule that is able to predict the hysteresis behavior and fracture of WF brace members. Finite element analysis for two experiments out of half a dozen of tests designed by the first author and conducted by Richard (2009) is presented. Brace behavior and failure modes are described with a refined fracture rule detecting fracture. Further, the effect of having small-size holes or up to ± 20 °C difference in temperature were investigated. However, these finite element models are often computationally expensive and may not be suitable for simulating the response of large structures. Therefore, investigation of the sensitivity of hysteresis loops and fracture life to changes in mesh density, number of integration points and using full or reduced integration elements is carried out in this study so as to develop numerically stable and computationally efficient finite element models. Recommendations to future finite element modeling and experimental testing are presented.

Figure 1. Inverted V-shaped (chevron) WF steel brace members in framed structures (Photo courtesy of Professor Stephen A. Mahin)

2. SPECIMENS AND LOAD PROTOCOL

Specimens W4 and W6 of the Tremblay et al. (2008) and Richard (2009) tests were designed and detailed according to the AISC 2005b and the AISC seismic provisions (2005), respectively. Both WF-section specimens are made of ASTM A992 steel with a nominal yield strength of 345 MPa and an ultimate strength of 450 MPa. The optimal brace inclination angle for maximum shear rigidity with respect to the horizontal axis of Tremblay et al. (2008) and Richard (2009) tests is θ = 35° (Moon et al. 2007). The far end connections of the specimens represent the same scenario of the connections of a brace inside a frame in practice. The minimum possible gusset plate thickness was used with the working point being located at the intersection of the beam bottom flange and the adjacent column inner flange to minimize the gusset plate dimensions (Sabelli 2005). The gusset was assumed to be welded to the beams and columns and detailed to accommodate inelastic rotation associated with brace buckling. Cover plates were used to reinforce the net-section at connection regions. A doubler plate was used to connect the web to the gusset plate. The results of design are shown in Appendix. These tests were designed according to the weak-brace strong-gusset approach while verifying all possible failure modes. The effective length factor used in design is equal to 0.9.

Specimens W4 (W310×97) and W6 (250×115) with an effective slenderness ratio of 60, have width-to-thickness ratios of 7.5 and 5.9, respectively. These WF specimens were tested inside a 12 MN capacity frame in both tension and compression with 1.5 MN capacity in bending that is available at the Structural Engineering Laboratory at the Ecole Polytechnique of Montreal. Sample specimen inside the test frame is shown in figure 2(a). In practice, these section sizes are commonly used in braced steel frame construction located in high seismic regions.

The two WF specimens were subjected to a far-field quasi-static cyclic loading protocol. The loading protocol was developed in terms of interstorey drift. The cyclic loading protocol is shown in figure 2(b). The normalized axial deformation of the brace is equal to the applied axial displacement divided by the length between the plastic hinges at the ends of the brace. Loading protocol is a symmetrical applied
displacement history with stepwise incremented amplitudes that simulates the demand imposed by earthquakes at distance. Up to cycle number twenty, the loading history reproduces the median demand that is anticipated for moderately ductile braced steel frames designed according to CSA-S16 (2009) when subjected to earthquake ground motions compatible with the 2% in 50 years seismic hazard level (Izvernari et al. 2007). Additional two cycles with a smaller amplitude are applied. The two small amplitude cycles are followed by cycles of increasing amplitudes to reproduce the demand expected in higher seismic regions (Fell et al. 2006).

3. FINITE ELEMENT ANALYSIS WITH MATERIAL AND FRACTURE MODELS

Nonlinear finite element analyses were performed using the Abaqus code (2011). Four node quadrilateral shell elements with nine integration points through their thickness were used. The Simpson’s integration rule was used. The combined isotropic-kinematic hardening material model with data type equal to half a cycle was used in the analysis of the two specimens. In the finite element analysis, the connection plates and the doubler plate used in Tremblay’s and Richard’s WF tests were connected to the flanges and the web of the WF brace using tie-type multi-point constraints, and the weld was not modeled. All elements were given the same nominal elastic modulus (200 GPa) and Poisson’s ratio (0.3). Fixed boundary conditions were applied at the ends of the braces. The brace with end connections are shown in figure 3 for specimens W6 of Tremblay et al. (2008). The input file was adjusted manually for the material model and the geometric imperfection.

Figure 2. a) Specimen W4 inside test frame (Photo courtesy of Richard 2009), and b) Loading protocol for specimens W4 and W6 used in experiments and finite element analysis

Figure 3. Brace with end connections for specimen W6
The finite element analysis was performed in two stages. In the first stage, first modal shape was obtained through linear buckling analysis while multiplying the mid-length out-of-plane displacement by an initial imperfection value that is equal to 0.002. In the second stage, the analysis of the model with all cyclic steps of applied axial displacements was performed. In the analysis, equilibrium was satisfied at each increment within the cycles using the Newton-Raphson method.

The combined isotropic-kinematic hardening is capable of simulating the expansion, contraction and the shift of the yield surface in stress space to model the brace behavior under cyclic loading. The yield surface is defined by the function

\[ F(\sigma_{ij}, k) = \frac{1}{\sqrt{2}} (S_{ij} - \alpha_{ij}^d) (S_{ij} - \alpha_{ij}^d) - \bar{\sigma}_y = 0 \]  

(1)

where \( \sigma_{ij} \) is the stress tensor, \( \alpha_{ij}^d \) is the deviatoric part of the backstress tensor \( S_{ij} \), \( \bar{\sigma}_y \) is a stress quantity associated with the expansion or contraction of the yield surface

\[ \bar{\sigma}_y = \sigma_{yo} + k \]  

(2)

where \( \sigma_{yo} \) is the initial yield stress, and \( k \) is the hardening parameter that defines the size of the yield surface.

In Equation (1), the translation of the yield surface \( \alpha_{ij}^d \) is subtracted from the corresponding stresses \( S_{ij} \).

The increment of the deviatoric part of the backstress tensor according to Ziegler’s [35] kinematic hardening rule is defined as

\[ d\alpha_{ij}^d = \frac{H}{\bar{\sigma}_y} (S_{ij} - \alpha_{ij}^d) \, d\varepsilon_{eq}^p \]  

(3)

where

\[ H = \frac{d\sigma}{d\varepsilon^p} \]  

(4)

The translation in the yield surface is a function of the plastic hardening modulus, the initial yield stress and the plastic strains. The translation of the yield surface can be subtracted from the corresponding stresses according to different kinematic hardening rules. The Ziegler’s (1959) kinematic hardening rule is used in the finite element analysis presented here. However, the tangent or plastic hardening modulus, \( H \), is adjusted through a trial and error procedure to obtain the hysteresis response that is a reasonable match to the experimental response.

The increment in plastic strain according to the associated flow is defined as

\[ d\varepsilon_{ij}^p = \frac{\partial f}{\partial \sigma_{ij}} \, d\varepsilon_{eq}^p \]  

(5)

where the increment of the equivalent plastic strain is defined as

\[ d\varepsilon_{eq}^p = \sqrt{\frac{2}{3}} d\varepsilon_{ij}^p \, d\varepsilon_{ij}^p \]  

(6)

The incremental deformation plasticity theory in a strain based approach, based on the significant cumulative plastic strain, was implemented in the refined fracture model to predict the behavior and fracture life of the braces. On the demand side, the significant cumulative plastic strain is defined as:

\[ \varepsilon_p \text{significant} = \sum_{\text{tensile}} f_{\varepsilon_1}^{\varepsilon_z} \exp\left[1.5\varepsilon_1\right] d\varepsilon_t - \sum_{\text{compressive}} f_{\varepsilon_1}^{\varepsilon_z} \exp\left[1.5\varepsilon_1\right] d\varepsilon_c \]  

(7)
where $T$ is the triaxiality ratio which is defined as the mean stress over the effective or the von Mises stress.

In Equation (7), the equivalent compressive strains are subtracted from the equivalent tensile strains and added incrementally for every cycle of loading. The significant plastic strains are taken to be tensile minus compressive strains. It should be mentioned that there was no difference in the number of fracture life cycles when considering tensile minus compressive strains or compressive minus tensile strain. Strains are considered compressive when triaxiality is negative.

The degradation of the monotonic tensile capacity during cyclic loading on the capacity side is computed according to the equation:

$$\eta_{\text{cyclic}} = \exp(\lambda_{\text{CVGM}}\varepsilon_p) (\int_0^{\varepsilon_p} \exp^{1.5T} d\varepsilon_p)_{\text{monotonic}}$$

(8)

where $\varepsilon_p$ is the cumulative plastic strain, and $\lambda_{\text{CVGM}}$ is the degradation coefficient of the monotonic capacity.

In Equation (8), the values of both monotonic capacity and $\lambda_{\text{CVGM}}$ were calibrated based on results of the notch round bar monotonic and cyclic experiments (Kanvinde and Deierlein 2004). These tests were accomplished using several types of steel under two different loading histories. For the first loading history, the specimen were subjected to constant amplitude high tension and/or low compression cyclic displacements to failure. For the second loading history, the specimen were subjected to constant predefined low tension and/or compression amplitude displacements, and then pulled to failure during the last tension cycle.

4. VALIDATION OF EXPERIMENTAL TESTS

Specimen W4 was selected from the brace tests to validate the finite element model. The dimensions of the specimen are shown in Appendix. The measured free length of the gusset plates was equal to two times the gusset plate thickness as suggested by Astaneh-Asl et al. (1985) to allow for the free formation of plastic hinges in the end gussets and this interns will reduce the demand on the mid-length plastic hinge. Similar to the other specimen, W4 was made of ASTM A992 Grade 345 steel and the plates used as gusset, connection, and doubler were all made of ASTM A572 Grade 345 steel. The experimental and equivalent finite element axial hysteretic loops are shown in figure 4. In this figure, the normalized axial displacement is equal to $\delta/L_H$ and the normalized axial force is equal to $P/A_y F_y$. In these expressions, $\delta$ is the axial applied displacement; $L_H$ is the length between the plastic hinges at the ends of the brace; $P$, $A_y$, and $F_y$ are load resistance, cross-sectional area, and yield strength of the WF brace member, respectively.

Specimen W4 was subjected to eight elastic cycles of compression-tension loading before the occurrence of a bow-shape buckling during the compressive side of cycle number nine in both the experiment and the finite element analysis. Two plastic hinges formed progressively in the free length of the two gusset plates. The full tension side of cycle number nine straightened and yielded the specimen. The compressive loads showed a marked decrease in capacity in the cycles following cycle number nine in both the experiment and the finite element analysis. This is attributed mainly to the residual elongation, or camber, and to the formation of plastic hinges at both ends of the specimen. The bow-shape buckling became larger during the compressive side of cycle number ten. The two free length plastic hinges formed completely during the same cycle. Similar plastic behavior of the gusset plates was evident at all times during all the following cycles in the finite element model.

Local buckling at the mid-length plastic hinge began to occur during the compressive side of cycle number twelve with inward bulging of the specimens’ compressive flanges. This inward bulging mostly disappeared during the tension side of the same cycle. Upon full tension loading, a small wave-shape curving at the tips of tension flange was noticed in the finite element analysis. During the following cycles, local buckling continued to increase in severity. Consequently, the buckling shape transformed
from a bow-shape to a kinked-shape as shown in figure 5 for specimen W6, for instance. A noticeable reduction in brace strength and stiffness occurred during the two smaller amplitude applied cycles (21 and 22). Local buckling occurred during the compressive side of cycle number twenty five for specimen W6.

Factors that affect the shape of local buckling are the location and direction of bows present after the steel cools and is later cold straightened; the increase in hardness, yield strength and ultimate strength of the k-area; and the residual stress distribution. WF brace members could have three possible locations of failure initiation at mid-length plastic hinges, specifically at the compressive flanges, the tension flanges, or the k-area. Failure at the compressive flanges is commonly seen in cyclic tests of WF braces.
Compressive flanges can buckle locally outward or inward. Whether the compressive flange buckles outward or inward depends on the initial wavy shapes of those flanges at the early stage of mid-length plastic hinge formation. In the finite element models and the experiments, outward and inward bulging of the web is also noticed. High stress concentration can be present at the web of the mid-length plastic hinge upon tension loading. The concentration usually results in cross-line cracks of the web during experiments.

It should be mentioned that the finite element model presented here was able to simulate the shift in location of the mid-length plastic hinge. It was noted that local buckling formed at a distance of less than the depth of the WF from the mid-length of the specimen as shown in figure 5 in both the experiment and the finite element analysis. The variation of the yield strength and ultimate strength at the cross sectional level and along the length of the WF brace affects the location of plastic hinge and its formation in addition to the asymmetry in the geometry of the gusset plate end conditions. This shift is believed to be affected by initial imperfections. For example, the maximum initial imperfection may not be at the mid-length of the specimen.

5. DISCUSSION OF RESULTS

5.1 Mesh Density Analysis and Integration Points

Six mesh densities were used at the location of the mid-length plastic hinge with element sizes equal to 0.5, 0.75, 1.0, 1.5, and 2.0 the thickness of the flanges. For all mesh densities, the shell elements have an aspect ratio of unity. There is a rate of increase in the cumulative plastic strain when decreasing the size of the element as shown in figure 6(a). In the current study, a mesh size equal to half the thickness of the flange was used in all analyses. The choice of each of previous element sizes did not alter the hysteresis behavior of the WF brace members. In one instance, the compressive resistance was overestimated in the finite element model when using a mesh size of 0.75 the thickness of the flange for specimen W6 as shown in figure 4.

The finite element analysis were conducted when using various numbers of integration points (3, 5, 7, 9, 15) using Simpson's integration rule as shown in figure 6(b). Two cases are considered in the analyses here. In the first case, the analysis was conducted with an element size equal to half the thickness of the flange. In the second case, the analysis was conducted with an element size equal to thickness of the flange. There was negligible difference in the hysteresis behavior when using 5, 9, 7, or 15 integration points, suggesting that a minimum number of 5 integration points could be used to produce hysteresis behavior that matched the experimental result. Nine integration points through thickness of the reduced integration shell elements were used in the current study. It should be mentioned that using full or reduced integration elements did not alter the hysteresis behavior or the fracture life of the WF brace members.

![Figure 6](image)

Figure 6. a) Variation of cumulative plastic strain with mesh density, and b) Effect of number of integration points on the axial hysteresis behavior using Simpsons (S) rule for W6
5.2 Fracture Life

A calibrated cyclic void growth model (CVGM) for ultra-low cycle fatigue was used in the finite element analyses to predict fracture based on the significant cumulative plastic strains in the previous two cases of element sizes. For the current modeled W4 and W6 specimens, the monotonic tensile capacity was of an average value of 2.90 when using an element size of half the thickness of the flange. This average value was equal to 3.0 when using element size that is equal to the thickness of the same flange. These average values are similar to the values obtained by Kanvinde and Deierlein (2004) for AW50 (ASTM A572 Grade 50) steel. In both cases of element sizes, the previous monotonic capacities degrades with a degradation coefficient of 0.11, obtained by trial and error, to predict the initiation of fracture of the WF braces. Fracture initiation in W4 and W6 is shown in figure 7 when using element size of half the thickness of the flange. In both experiment and finite element analysis, fracture initiation occurred during cycles 23 and 31 for specimens W4 and W6, respectively. The fracture life of the WF braces modeled in the current study is not affected by the increase in the mesh density beyond the thickness of the flange. However, this increase in the mesh density significantly increased the time of simulation.

![Figure 7](image)

Figure 7. Fracture prediction for a) W4, and b) W6

Gusset plates were designed and detailed with their minimum possible thickness to develop the tensile capacity of the WF section in tension without buckling in compression. Further uncontrolled increase in the gusset plate's tensile resistance reduces the fracture life of the brace (Haddad et al. 2011). However, a slight increase in the thickness of gusset plates may be necessary to avoid undesirable fracture of gusset plates especially when frame action is considered in design of these gussets. The Whitmore width occurs at an angle of 30 degrees. Research by Hsiao et al. (2012) showed that frame deformation effects in tension have caused an increase in the von Mises and the first principal stresses located at the Whitmore section of the gusset as well as the increase in combined effects of factored tension and shear forces at the gusset edges. A previous research by Cheng et al. (2000) demonstrated that frame effects in compression have caused a reduction in the capacity of the gusset plates. It should be mentioned that crack initiation was detected at the same number of cycles (cycles 23 and 31 for specimens W4 and W6, respectively) when increasing gusset plate thickness by 1.588 mm (1/16 in) or even by 3.175 mm (1/8 in), suggesting that a slight increase in the gusset thickness would not affect the crack initiation limit state.

5.3 Effect of Small Size Holes

The effect of having a small-size circular hole up to 4 mm in diameter had no effect on hysteresis behavior and fracture life of the WF bracing members. These small size holes were located at the centerline of either the web or half of the flange at the mid-length of the WF brace. The shift in location of the mid-length plastic hinge occurred at the same location for WF braces that do not have the small-size hole in the finite element analysis. Haddad and Tremblay (2006) investigated the effect of having a 10 mm triangular notch at the end of the slot hole to study the performance and sensitivity of the net-section connection of an HSS brace member to fabrication defects. The maximum equivalent plastic
strain occurred in the vicinity of the notch located in the wall of the HSS section. More research is needed to investigate the effect of the small size fastener holes on the hysteresis behavior and fracture life of the WF braces through experimental testing and finite element modeling.

5.4 Effect of Temperature

The effect having up to ± 20 °C difference in temperature did not alter the hysteresis behavior of these braces. However, accumulated heat that is generated from the movement of dislocations at the localized locations of mid-length plastic hinge during cyclic loading will alter the hysteresis behavior of the WF braces. Therefore, it is recommended to consider the heat release that is generated during cyclic loading in any future finite element analysis when converting the engineering stress-strain curves obtained from coupon or stub-column tests at room temperature to true stress-strain curves. This heat is believed to affect the mechanical properties, specifically strength and both elastic and plastic-hardening modulus. Reduction in the tangent modulus is basically needed to produce accurate simulation models for hysteresis behavior that is obtained by a trial and error procedure for the two braces in current study. Moreover, the localized fracture initiation through the thickness of the flanges will be affected by the heat distribution there. It should be mentioned that the rate of heat increase for tubular braces is believed to be significant compared with that for WF braces at the localized locations of mid-length plastic hinge. The geometric nature and behavior of the mid-length plastic hinge is different for previous two types of sections. Therefore, ultra-low cycle-fatigue tests on small-scale coupons are recommended to optimize the stress-strain curves of braces. These tests could be taken from flanges, webs and corners of the brace members. The accumulated increase in heat can promote the movement of dislocations during cyclic loading and this interns reduces the tangent modulus of the stress-strain curves that is needed for simulation of full-scale brace members. The smaller the tangent modulus, the earlier the occurrence of local buckling for braces under cyclic loading (Haddad 2015).

6. CONCLUSIONS AND RECOMMENDATIONS

This study demonstrated that the current finite element model including the refined fracture model can accurately simulate hysteresis responses and predict fracture of WF brace members. In addition, it was found that further increases in mesh density by reducing element size beyond thickness of the flange did not alter both hysteresis behavior or fracture life but increased the time of simulation. However, reducing mesh density way beyond thickness of flange resulted in numerical error. The effect of using full or reduced integration elements on the hysteresis behavior and fracture life is negligible. In addition, the effect of number of integration points on hysteresis behavior is negligible for cases of using five integration points or more. These findings may be useful in the development of other full-scale finite element models where an appropriate element size, using full or reduced integration elements and number of integration points are required in analysis of frames of multistorey buildings without substantially increasing the computational cost.

A comprehensive review is needed for the current ultra-low cycle-fatigue (ULCF) models that are presented in literature while considering the effect of accumulated increase in heat that is generated by the movement of dislocations, on the tangent modulus in addition to strength and plastic strains. The accumulated increase in heat can promote the movement of dislocations during cyclic loading, lowering both strength and tangent modulus, and altering strain measurements.

7. ACKNOWLEDGMENTS

The study was made possible by financial support of the Individual Research Grant of the UAEU (SEED – G00001044) and the UAEU Program for Advanced Research (UPAR – G00001916) fund. In addition, many thanks to the UAE University for providing the computational modeling facilities to conduct the work in this study.
8. REFERENCES


CSA. CSA-S16-09, Design of Steel Structures, Canadian Standards Association, Mississauga, ON, Canada, 2009.

Fell BV, Kanvinde AM, Deierlein GG (2010). Large-scale testing and simulation of earthquake induced ultra low cycle fatigue in bracing members subjected to cyclic inelastic buckling *Report No. 172*, Department of Civil and Environmental Engineering, Stanford University, San Francisco, CA.


Gugerli H, Goel SC (1982). Inelastic cyclic behavior of steel bracing members *Report No. UMEE 82R1*, Department of Civil Engineering, University of Michigan, Ann Arbor, Michigan, USA.


Civil Engineering, University of Washington, Seattle, USA.


Notes: Fini: aucun
Plaques: ASTM A572, gr. 345
CVN 27J at 20°C si t > 51 mm
Soudure: E490 XX
Qté: 1 (1186 kg)