Ultra-Low Cycle Fatigue Fracture Life of a Type of Buckling Restrained Brace

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ABSTRACT

Buckling restrained braced frames (BRBFs) for seismic load resistance have been widely used in recent years. One of the key requirements for a buckling restrained brace is to sustain large plastic deformations under severe ground motions. The core of a buckling restrained brace is prone to fatigue fracture under cyclic loading. The earthquake induced fracture type of the core plate in a buckling restrained brace can be categorized as ultra-low cycle fatigue fracture. This paper investigates the ultra-low cycle fatigue fracture life of a type of composite buckling restrained brace previously tested. The newly developed cyclic void growth model was adopted to theoretically predict the fracture and crack initiation in the core. In addition, the Coffin-Manson fatigue damage model was applied to estimate the fracture life of the brace. A FEM model of the BRB developed in ABAQUS was used to evaluate the fatigue life. The analysis results showed that the cyclic void growth model is capable to nearly predict the fracture life of the core in buckling restrained brace.

1. Introduction

Buckling restrained braced frames (BRBFs) for seismic load resistance have been widely used in recent years. A BRBF differs from a conventionally braced frame because it yields under both tension and compression without significant buckling. Most buckling restrained brace (BRB) members currently available are built by inserting a steel plate into a steel tube filled with mortar or concrete. The steel plate is restrained laterally by the mortar or the steel tube and can yield in compression as well as tension, which results in comparable yield resistance and ductility, as well as a stable hysteretic behavior in BRBs. A large body of knowledge exists regarding conventional BRBs’ performance which can be found in the works by Black et al. [1], Inou et al. [2], Qiang [3], Watanebe et al. [4], Tremblay et al. [5], Usami et al. [6,7], Hoveidae et al. [8-10], Chou et al. [11], Eryasar et al. [12]. As shown in Fig. 1, a typical BRB member consists of a steel core and a buckling restraining mechanism (BRM), which inhibits the core local and global buckling.
A variety of arrangements have been proposed for BRBs. Hoveidae et al. [10] proposed a novel all-steel BRB called short-core buckling restrained brace (SCBRB) in which a shorter core plate was used and then the seismic response of SCBRB was evaluated through nonlinear time history and finite element analyses. Bazzaz et al. [13] proposed a new type of non-buckling steel ring dissipater in off-center and centric bracing systems in order to enhance the ductility of braced system. Moreover, Bazzaz et al. [14] investigated the behavior of off-center bracing system with ductile element (circular dissipater), in order to providing replacement of damaged member without needing repair or reconstruction of the general system. The analytical results revealed that the performance of steel ring at the end of off-center braced system illustrating as a first defensive line and buckling fuse in the off-center bracing system. Andalib et al. [15] analytically and experimentally studied the usage of steel rings made of steel pipes as an energy dissipater at the intersection of braces. These studies showed that the brace with the steel ring exhibits a steady and wide hysteresis curve and a tensile ductility factor of 8.68 was achieved. Furthermore, Bazzaz et al. [16] proposed a new bracing system using circular element (circular dissipater) in order to replace damaged member without needing rehabilitation or repair of general system. Using nonlinear software package ANSYS, a frame with off-center bracing system with optimum eccentricity (OBS-C-O) and another frame with same specifications without circular element (OBS) was created. The analytical results and comparison between plots of these two models showed that the first model has higher performance than the others. Ozcelik et al. [17] investigated the response of BRBs with different types of restraining members. In the mentioned studies, additional end restraints were added at the unrestrained part of the core plate at both ends, isolation material was employed, and a more economical casing member was used. The energy dissipation capacity of the BRBs was found to be significantly dependent on compression strength adjustment factor, $\beta$, and strain hardening adjustment factor, $\omega$. The results showed that the improved BRBs with sufficient stiffness to resist out-of-plane buckling at both ends have acceptable cyclic performance according to the test results. Razavi et al. [18] proposed a type of all-steel buckling restrained braces called reduced length buckling restrained brace (RLBRB), in which a shorter core was sandwiched between a restraining member. The test results showed that the reduction in BRB core length and consequently the increase in strain of core up to amplitudes of 4-5% enhance the risk of low-cycle fatigue failure. The low cycle fatigue response of the core plate was examined by Coffin-Manson fatigue damage criteria as well. Wang et al. [19] surveyed the low cycle fatigue behavior of all-steel buckling restrained braces. Experimental and numerical studies on the effect of stoppers on the low-cycle fatigue performance of buckling-restrained brace to develop the high-performance BRB used in bridge engineering were conducted. According to the mentioned experimental results, the BRBs with stoppers possess a higher low-cycle fatigue performance than those without stoppers.
Yan-Lin et al. [20] proposed a new type of BRBs namely core-separated buckling-restrained brace (CSBRB), and theoretically and experimentally investigated the behavior of the brace. The results showed that the material utilization efficiency of the CSBRB is significantly improved compared with common BRB, since its cross-section spreads outwards by spacing two cores, thus improving the flexural rigidity of the restraining system. Most of research areas in the literature focuses on seismic response of BRBs and the lack of studies on low cycle fatigue response of BRBs is evident.

Typically, a core plate in a BRB is made up of a ductile steel rectangular plate. The core plate is normally designed according to code-based forces. In general, the limit state of a BRB is the core fracture at mid-length or at the core ends close to transition zones, depending on core details. If a stopper is provided on the core plate to prevent the slippage of restraining member, the core plate tensile fracture is likely to take place at a region near to the stopper [11]. However, in some cases, especially for the BRBs without stopper, the fracture tends to attend the core ends [18]. The fracture of the core plate in a BRB can be classified as a low cycle fatigue or ultra-low cycle fatigue fracture problem, depending on the loading history applied.

Normally, extreme loads applied to steel structures can yield either to monotonic ductile failure or to fatigue failure at very small number of cycles (<100 cycles). This fatigue regime is called ultra-low cycle fatigue (ULCF), in order to distinguish it from low-cycle fatigue (LCF), since ULCF damage mechanisms are distinctive of those typical from LCF. The ULCF fits between the monotonic ductile damage and LCF damage and exhibits damage features from both damaging processes [21]. Earthquake induced fracture can be categorized into ultra-low cycle fatigue (Shimada et al. [22], Kuroda [23], Nip et al. [24]), which is characterized by large inelastic strain amplitudes (several times of the yield strain) and extremely few cycles to fracture (Zhou et al. [25]). ULCF is quite different from conventional high cycle fatigue (requires more than $10^4$ cycles to failure where stresses are below the yield strength) or low cycle fatigue ($10^2$ to $10^4$ cycles to failure where strains are in excess of yield). More importantly, the ULCF fracture is often the governing limit state in steel structures subjected to severe earthquakes. The extremely random loading histories of ULCF associated with very few cycles make them difficult to adapt to techniques developed for high and low cycle fatigue, such as rain-flow cycle counting method (Downing et al. [26]) and strain-life approaches proposed by Manson [27] and Coffin [28]. A number of fatigue damage models, proposed by various authors are available in the literature. With respect to ULCF modelling, existing approaches reported in the literature may be classified into coupled and uncoupled models. This classification is usual in monotonic ductile models. Coupled models consider interdependency between plasticity and damage and allows linear or non-linear damage evolution. The coupled plasticity-damage models allow the simulation of the crack initiation (damage onset) and crack propagation (damage spread). An example of these formulations was proposed by Lemaitre [29]. There are some propositions in literature for uncoupled ULCF models, which are supported by distinct physical assumptions. Xue [30] proposed an exponential damage rule for fatigue life prediction in the ULCF regime, which overcomes the overestimation limitation of the classical Coffin–Manson approach that has been cited in the literature (Pereira et al. [21]). The ULCF model based on the cyclic behavior of micro-voids, proposed by Kanvinde and Deierlein [31] can also be classed as an uncoupled damage model, which postulates the material degradation, by micro-void growth, as a function of the plastic strain weighted with a triaxiality function.

Kanvinde and Deierlein [31] suggested an explanation for the processes which may govern micromechanical ULCF behavior during earthquake-type cyclic loading. The resulting continuum-based, Cyclic Void Growth Model (CVGM) has shown promise in simulating the physical void growth, shrinkage and damage events that lead to ductile fracture initiation during cyclic loading in small-scale experiments. The model has been rigorously examined by Kanvinde and Deierlein [31] at the small-scale across a variety of steel types, geometries and loading histories. Despite the low cycle fatigue response of BRBs is investigated in some prior works, which can be found in the literature, the ultra-low cycle
fatigue response is not deliberated meticulously. In this paper, the CVGM is applied to a BRB previously tested by Chou et al. [11] in order to predict the ultra-low cycle fatigue life. In addition, the fracture life of the core plate is examined by the well-known Coffin-Manson damage rule and compared with that evaluated by CVGM model.

2. CVGM formulation

Based on the researches by Kanvinde and Deierlein [31], the two most important processes to capture in modeling ultra-low cycle fatigue (ULCF) fracture are void growth “demand,” including the effects of void growth and shrinkage/collapse under reversed cyclic loading, and degraded void growth “capacity,” associated with cyclic strain concentrations of the inter void ligament material. These are the two key aspects of the proposed CVGM that distinguish. The proposed cyclic void growth model to simulate ULCF advances concepts described by Rice and Tracey [32], Hancock and Mackenzie [33] for monotonic loading, and Ristinmaa [34] and Skallerud and Zhang [35] for cyclic loading. The underlying mechanisms of low cycle fatigue fracture involve cyclic void growth, collapse, and distortion. Fig. 2 represents the ductile fracture mechanism in metals based on CVGM model. In addition, a fractograph of ULCF is displayed in Fig. 3 [36].

First, this paper aims to briefly review a commonly accepted monotonic void growth model that forms the basis of the proposed cyclic void growth model. For a single spherical void in an infinite continuum, the void growth rate can be described by the following equation (Rice and Tracey, [32]):

\[
\frac{dR}{R} = C \exp(1.5T) d \varepsilon_p
\]

(1)

where \( R \) is the average void radius; \( T \) represents the stress triaxiality which is the ratio of mean stress to effective stress and \( C \) is a material parameter. In addition, \( d \varepsilon_p \) denotes the incremental equivalent plastic strain as defined in Eq. (2).

\[
d \varepsilon_p = \sqrt{\frac{2}{3}} d \varepsilon_{ij} d \varepsilon_{ij}
\]

(2)

Integrating Eq. (2), the void radius \( R \) (expressed in terms of a ratio with respect to the initial void radius) can be expressed as:

\[
\ln \frac{R}{R_0} = \int_0^{\varepsilon_p} C \exp(1.5T) d \varepsilon_p
\]

(3)

where \( R_0 \) denotes the initial void size. Assuming void growth to be the controlling aspect of the fracture process, fracture is calculated to occur when the void ratio attains a critical size, i.e.:}

\[
\ln \frac{R_{\text{critical}}}{R_{\text{monotonic}}} = \int_0^{\varepsilon_p^{\text{critical}}} C \exp(1.5T) d \varepsilon_p
\]

(4)

This calculation can be further simplified to express a fracture criterion through a void growth index.
(VGI_{monotonic}), which is compared to its critical value, \( VGI_{critical\ monotonic} \) expressed as follows:

\[
VGI_{monotonic} = \int_0^{T} \exp(1.5T)d\varepsilon_p > VGI_{critical\ monotonic} = \ln\left(\frac{R_0}{R_0^{critical\ monotonic}} / C\right)
\]

This forms the basis of the void growth model in which \( VGI_{critical\ monotonic} \) is treated as a material property that is invariant to stress and strain states (Kanvinde and Deierlein, [31]). Kanvinde and Deierlein extended their idea to cyclic loads and developed a new version of void growth model (VGM) currently known as the cyclic void growth model, CVGM. The backbone of CVGM is identical to that of VGM, however the model is refined as outlined below to numerically capture the effects of reversing loads on the growth and coalescence phases. They assumed that a macro-crack initiates when the Eq. (6) is satisfied over the characteristic length \( l^* \) [37]:

\[
VGI_{cyclic} > VGI_{critical\ cyclic}
\]

(6)

Where \( VGI_{critical\ cyclic} \) is the critical cyclic void growth index. Considering void shrinkage during compressive (negative) triaxialities, Eq. (5) has been upgraded to the following form:

\[
VGI_{cyclic} = \sum_{Tensile\ cycles} \int e^{[0.5T]}d\varepsilon_p - \sum_{Compressive\ cycles} \int e^{[0.5T]}d\varepsilon_p
\]

(7)

It is supposed that the critical cyclic void growth damage index can be assessed from its monotonic counterpart as follows:

\[
VGI_{critical\ cyclic} = VGI_{critical\ monotonic} \cdot \exp(-\lambda \varepsilon_p)
\]

(8)

The \( VGI_{cyclic} \) demand is calculated based on stress and strain histories recovered from FEM analyses. The parameter \( \lambda \) is treated as a material-dependent damageability coefficient. The cumulative equivalent plastic strain can be calculated at the beginning of each tensile excursion for a given material point and substituted into Eq. (8) to determine the current value of the critical void growth index \( VGI_{critical\ cyclic} \). This is compared according to Eq. (8) to the void growth “demand” \( VGI_{cyclic} \), calculated per Eq. (7), to check for the fracture limit state (Kanvinde and Deierlein, [31]).

An examination of Eqs. (5) to (8) indicates that the accuracy of the CVGM relies on how precise the parameters \( VGI_{critical\ monotonic} \) and \( \lambda \) are calibrated. Lacking the data for the characteristic length of the ULCF, Kanvinde and Deierlein [31] adopted those of monotonic ductile fracture. The calibration of the critical monotonic void growth index requires first, conducting monotonic uniaxial tests on notched round bars of various notch radii, next, calculating the right hand side of Eq. (7) by the FEM, and finally, comparing these numerical and experimental results [37]. According to this explanation of crack propagation mechanism, each macro-crack can be considered to be an assembly of smaller cracks created in several consecutive steps. If the length of such micro-cracks is considered equal to \( l^* \), then the problem of simulating crack propagation reduces to a series of micro-crack creation problems. As discussed earlier, based on CVGM, during ULCF in steel material, damage initiates when Eq. (6) is met over an element with the characteristic length of \( l^* \). Thereafter, when cracking occurs in an element, adjacent elements become more vulnerable to damage initiation and the process of cracking expedites for them as a redistribution in stress and strain happens in the vicinity of cracking zone [38].

3. Coffin-Manson damage model

Coffin [30] and Manson [29] proposed an empiric relation, which has been widely used for LCF, as follows:

\[
\varepsilon_f = \varepsilon_0 N_f^m
\]

(9)

Eq. (9) is represented by a linear relation in a bi-logarithm diagram, where \( \varepsilon_f \) and \( N_f \) are uniaxial plastic strain amplitude and the number of cycles to failure, respectively. \( \varepsilon_0 \) is the fatigue ductility coefficient and \( m \) is the fatigue ductility exponent. Some authors such as Tateishi et al. [39] have shown that the Coffin–Manson relation does not give a satisfactory description of the ULCF regime, for many metals. They report a fatigue life
over prediction when the Coffin–Manson relation is used in ULCF domain. However, this issue is going to be examined in this paper by selecting the fracture life predicted by CVGM as a benchmark. A number of equations can be found in the literature which tries to predict the fracture life of BRBs [19]. In this paper the equation proposed by Nakamura et al. [40] as the most conservative equation is selected to estimate the fracture life of the BRB specimen throughout the loading history. Low cycle fatigue fracture of the brace in the core occurs when the damage index (\( DI \)) reaches to one. Damage for each amplitude of cycling is estimated by dividing the number of cycles at that constant amplitude \( (n_i) \) by the number of constant amplitude cycles at that amplitude \( (N_{fi}) \) necessary to cause failure, and overall damage due to low-cycle fatigue is estimated by linearly summing the damage for all of the amplitudes of deformation cycles considered. The well-known Miner's rule is used to accumulate damage during cyclic excursion of the core in a BRB. The fracture of the component subjected to different levels of strain demand will occur when the index \( DI \) reaches to 1 as follows:

\[
\sum \frac{n_i}{N_{fi}} = 1
\]  

(10)

\[
DI = \sum_{i=1}^{N} \frac{n_i}{N_{fi}} \left( \frac{\varepsilon_i}{\varepsilon_{0i}} \right)^{m}
\]  

(11)

Based on the equation proposed by Nakamura et al. [45] the parameters \( m \) and \( \varepsilon_0 \) are set to -0.490 and 0.2048, respectively.

4. Assessment of ULCF in BRBs

As mentioned previously, this paper aims to address the ULCF response of buckling restrained braces through the newly developed CVGM model. By knowing the predicted fracture time by CVGM, the damage index estimated by Coffin-Manson rule is also calculated at threshold of fracture and the ability of this method to capture the fracture in ULCF domain is evaluated. For this purpose, a sample composite buckling restrained brace recently tested by Chou et al. [11] is considered. In order to implement the CVGM method for fracture prediction of the core plate in the BRB specimen, ABAQUS [41] general purpose finite element software is employed. A Fortran UVARM subroutine is developed to calculate the damage indices at each integration point. Fig. 4 represents the characteristics of the BRB specimen tested by Chou et al. [11]. Table 1 summarizes member size of the BRB specimen. Core plate width, \( b_c \), and thickness, \( t_c \), were 150 mm and 22 mm, respectively. ASTM A36 steel with a nominal yield strength of 250 MPa was specified for the channels as restraining members, and ASTM A572 Grade50 steel was specified for the core, side, and face plates. The specified 28-days concrete strength was 35 MPa. Table 2 summarizes the material properties used in specimen 1. The test set-up of the BRB conducted by Chou et al. [11] is indicated in Fig. 5.
Fig. 4. Details and dimensions of the sample BRB

Table 1. Characteristics of the sample BRB (Specimen-1)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Core plate</th>
<th>Channel and face plates(mm)</th>
<th>No. of Bolts</th>
<th>Bolt spacing (mm)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>b₅(mm)</td>
<td>Lₕ(mm)</td>
<td>150x75x6.5x10</td>
<td>32</td>
</tr>
<tr>
<td></td>
<td>150</td>
<td>22</td>
<td>2800</td>
<td></td>
</tr>
</tbody>
</table>

Table 2. Material properties of the sample BRB (Specimen-1)

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Core plate (A572 Gr 50)</th>
<th>Channel</th>
<th>Face plate</th>
<th>Concrete strength (MPa)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>Fₛ(MPa)</td>
<td>Fᵤ(MPa)</td>
<td>Fₛ(MPa)</td>
<td>Fᵤ(MPa)</td>
</tr>
<tr>
<td></td>
<td>367</td>
<td>525</td>
<td>274</td>
<td>425</td>
</tr>
</tbody>
</table>
5. Numerical modeling of the BRB specimen

A three dimensional finite element model of the specimen-1 has been considered to assess the applicability of the numerical approach in this paper. The detail and dimensions of BRB model in numerical analysis corresponds to the BRB detail represented in Fig. 4. The connection portion of the braced was eliminated in the FEM model since the brace member was loaded axially. Material nonlinearity with the Von-Mises yielding criterion was considered in the steel core and restraining members. The elastic modulus and the Poisson ratio of steel material were set to 203 GPa and 0.3, respectively. A combined isotropic/Kinematic hardening model represented in Fig. 6a, was used for the steel material in order to accurately capture the cyclic response. The initial kinematic hardening modulus C and the rate factor γ were set to 2 GPa and 12, respectively. The calibration of hardening parameters signified in Fig. 6b, was conducted via comparison between the hysteretic curves obtained from the previous test by Tremblay et al. [5] and the FEM analysis conducted by the author. For isotropic hardening, a maximum change in yield stress of $Q_{\infty} = 160$ MPa and a rate factor of $b = 5$ were adopted.
Concrete infill as a part of restraining member was modeled with an elastic material with a Young modulus of 25 GPa and a Poisson ratio of 0.2 because it was expected to remain elastic during cyclic excursion of the core plate. Other parts of the brace including core plate, restraining channels, and the filler plates were modeled with eight-node solid elements (C3D8R). A fine mesh pattern was introduced in the core plate in order to accurately capture the strain history. However, coarser mesh was assigned to the other parts of the brace because they were expected to generally remain elastic. Rigid beams were also used to model the bolts connecting the restraining upper and lower segments. A contact interaction was used to capture the interface between the core plate and the restraining member during loading. Contact stabilization is based on viscous damping opposing relative motion between nearby surfaces without degrading the accuracy of the results. The contact automatic stabilization factor was set to 10^{-4}. Tangential contact behavior with a frictional coefficient of 0.1 was adopted to simulate a greasy smooth interface between the steel material of the core plate and the encasing. The same frictional coefficient was considered in the similar analysis conducted by Chou et al. [11]. An initial geometric imperfection was introduced in the model based on the data extracted from the first buckling model. The tie interaction was used to model the welding connection of the brace components. The brace was pinned at one core end and the axial displacement history was applied at the other core end. Nonlinear quasi static analysis including both material and geometric nonlinearity and initial and maximum increment size of 0.25 was conducted in ABAQUS 6.13. Full Newton method was assumed as the solution technique. In this paper, material parameters of CVGM model for the Grade50 steel material used for the core plate including $VGI_{monotonic}$ and $\lambda$ are assumed as 1.13 and 1.18 and the characteristic length $l'$ was set to 0.18mm, as proposed previously in a paper by Kanvinde and Deierlein [42]. It should be noted that, in the absence of enough laboratory test equipment, it may be possible to use the CVGM parameters based on the values reported in the literature, provided that the specification and mechanical properties of the selected steel material in the analysis and the one formerly calibrated through notched bar tests, closely match together. The ULCF prediction by CVGM model is strongly dependent on key parameters of material specially the $VGI_{monotonic}$ amount. Since it is extremely time-consuming to consider the characteristic length $l'$ for all of the elements in finite element model, only the critical zone in which the fracture is observed in experimental test is modeled with finer mesh. Fig. 7 shows the mesh generation at the critical zone of the core plate. As observed in the test, the core of BRB specimen 1 was fractured at the middle section during the cyclic loading. The BRB specimen in the test was positioned at an inclination of $\theta = 50^\circ$, with both ends sandwiched by dual gusset plates. However, it is modeled horizontally in the finite element program and the corresponding horizontal displacement history is applied at the brace end. The specimen is subjected to prescribed loading in Section T6 of the AISC seismic provisions [43]. The loading protocol has two phases. First, each specimen is subjected to an increasing axial strain history (called standard loading), defined at levels corresponding to core strains of 0.33, 0.52, 1.05, 1.58 and 2.1%. After the standard loading, the BRB specimen is subjected to additional loading protocol, including large deformation, fatigue test at a core plate strain of 1.6% up to failure. This type of loading history with large strain amplitudes and limited number of cycles can be classified in ultra-low cycle fatigue domain. Fig. 8 shows the loading protocol applied at the end of BRB model as was used in the test. In addition, Figs. 9a and 9b illustrate the finite element model of the entire BRB and also the restraining member, respectively. Moreover, the finite element model of the core plate and its mesh generation is illustrated in Fig. 10.
Fig. 7. FE Mesh generation of the BRB core at critical zone

Fig. 8. Applied loading protocol in the BRB test and FEM modeling

Fig. 9. a) FE model of the sample BRB, b) FE model of the restraining parts of BRB core including the channels, filler plates, face plates, and the concrete infill.
6. Finite element analysis results

The BRB model was subjected to a cyclic displacement history at the end and the hysteretic response of the brace was captured. Fig. 11 displays the hysteretic response of the BRB model during standard loading protocol and it is compared to the hysteric curve obtained from the test. As shown in Fig. 11, the hysteretic response of the BRB member in the test and the FEM model closely match together and then, the FEM model could properly capture the BRB behavior under prescribed condition and loading history.

As mentioned before, the CVGM is applied to validate the fracture in the BRB specimen. Based on test results, the BRB member tolerated large plastic deformations during standard loading and held a stable hysteretic behavior up to core strain of 2.1%. After the standard loading protocol, the brace was subjected to the fatigue loading sequence with a constant core strain of 1.6% up to failure. The test results showed that the core plate in the BRB specimen was fractured at the mid-section during the low cycle fatigue test and the fracture occurred at the beginning of 22th cycle of low cycle fatigue phase because of the crack initiation and evolution. Remarkably, the CVGM implemented on the FEM model of the BRB specimen is also able to predict the crack initiation and the fracture of the core plate at the same loading sequence and also the same site. The finite element analysis showed that the core fracture starts at the mid-length close to the core stopper. The fracture points in the test and also the FEM model are close together as shown in Figs. 12a and 12b. The contours in
Fig. 12b display the UVARM6 field-output which corresponds to the CVGM damage index (i.e. $\frac{VGI_{cycle}}{VGI_{critical}}$). As shown in Fig. 11b, the damage index evaluated by CVGM at the mid-length of the core and near to the stopper has the maximum value. The fracture is supposed to initiate when the damage index reach to 1. Therefore, the FEM model could properly predict the fracture site. Moreover, the evolution of CVGM demand and capacity over the characteristic length in the core and at the fracture zone is illustrated in Fig. 13. As shown in Fig. 13, the quantity of $VGI_{cycle}$ capacity decreases based on the accumulation of plastic strain at the beginning of each tensile excursion of loading and the value of $VGI_{critical}$ increases and decreases based upon the sign of triaxiality. The intersection point of the $VGI_{critical}$ and $VGI_{cycle}$ predicts the failure. As revealed in Fig. 13, the intersection point is close to analysis time 125s, which closely coincides with the beginning of the tensile sequence at 22th cycle of the imposed loading history. Therefore, the fracture time closely meet the fracture in the test and the CVGM could properly envisage the failure. Hence, in terms of the fracture site and the time, the FEM model and also CVGM could successfully predict the failure of the BRB in ultra-low cycle fatigue regime. For comparison, the fatigue fracture life of the core plate in the BRB was also estimated by Coffin-Manson relation based on the core strain history during the FEM analysis. The results showed that at the threshold of fracture predicted by CVGM, the Coffin-Manson damage index is just about 0.62 which is remarkably far from 1 (i.e. failure). Therefore, the Coffin-Manson damage criterion seems to overestimate the fracture life of the core plate in the BRB. Such a result can be found in the literature, emphasizing that Coffin-Manson damage rule can better capture the fracture in low to high cycle damage regimes which should be distinguished from ultra-low cycle fatigue regime.
7. Conclusions

In this paper, cyclic void growth model (CVGM) as a micromechanical-based fracture model is implemented to validate the ultra-low cycle fatigue fracture life of a buckling restrained brace. The assumed BRB specimen was previously experimentally examined and the fracture was observed after 21 cycles in the low cycle fatigue loading sequence which corresponded to the core strain of 1.6%. In this paper, the Finite element model of the BRB specimen was developed in ABAQUS together with a Fortran subroutine to predict the onset of fatigue fracture in the core. It is observed that the CVGM model is able to successfully predict the fracture initiation in terms of location and time (loading sequence) in the core plate and the results closely meet those observed in the test. As a result, it can be concluded that the cyclic void growth model acts as an acceptable model for predicting crack initiation and the failure of BRBs (core plate) in ultra-low cycle fatigue regimes. In addition, the results indicated that Coffin-Manson damage rule cannot properly predict the fracture in ultra-low cycle fatigue regime and is likely to overestimate the fracture life. More experimental tests together with FEM analyses are required to validate the CVGM model for ULCF damage prediction of BRBs with different arrangements and details.

8. References


[35]. Skallerud, B., and Zhang, Z. L. (2001). “On numerical analysis of damage evolution in cyclic