Effects of strain rate on the hysteretic behavior of buckling restrained braces

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Abstract: Two groups of geometrically-identical buckling restrained braces (BRBs) were subjected to either dynamic or quasi-static uniaxial loading until the cores fractured to investigate the effect of strain rate on the hysteretic behavior of BRBs. The test results show that the compression over-strength of the BRBs are significantly greater in dynamic loading than in quasi-static loading. This indicates that neglecting the strain-rate effect on BRBs is non-conservative. No obvious effect of strain rate on the cumulative deformation capacity of BRBs are observed in the test. The results are in support of a previously proposed empirical model for estimating the cumulative deformation capacity of BRBs.

Keywords: dynamic loading, compression strength adjustment factor, low-cycle fatigue, cumulative deformation capacity

1. Introduction

In a buckling restrained brace (BRB), a steel core is encased by and detached from a buckling restrainer, which is usually made from a steel tube filled with mortar. An unbonding material is generally employed between the core and the restrainer to minimize the axial load transferred to the restrainer when the core is connected to the building frame and subjected to axial loading. The core is expected to yield in both tension and compression without global buckling, thus yielding a stable and full hysteretic curve (Figure 1).

![Figure 1. Typical hysteretic curve of a BRB](image)

For steel itself, the effect of strain rate on its mechanical properties has been investigated since the 1940s (Manjoine 1944) and its effect in increasing the yield stress is generally conservatively neglected in the practical design of steel structures. For the same reason, BRBs are generally assumed as rate-independent. However, a few dynamic loading tests on BRBs have been reported (Hasegawa et al, 1999; Yamaguchi et al, 2002; Carden et al, 2004; Tremblay et al, 2006; Lanning et al, 2016). The results show that the strain rate effect on the yield strength of BRBs is similar and slightly more significant than that of steel itself. For cumulative deformation capacity, however, no definite conclusion can be drawn because of the limited number of dynamic tests. The only available data that explicitly demonstrated the strain rate effect on the cumulative deformation capacity of BRBs was reported by Carden et al (2004), in whose test a BRB subjected to dynamic loading exhibited 43% less cumulative plastic deformability as compared to its statically loaded counterpart.

In addition, the strain rate effect on the compression strength adjustment (AISC 341, 2010), which raises another important concern in the design of BRBs, has not been investigated in the past studies. Therefore, it is unjustified to consider it fully conservative to neglect the effect of strain rate on the hysteretic behavior of BRBs. This paper summarizes the results and observations of an experimental test series in which geometrically identical BRBs were subjected to either quasi-static or dynamic cyclic loading. In particular, the rate effect on the energy dissipation capacity and compression over-strength of BRBs are analyzed in detail.

2. Experimental program

Six BRBs were tested in the present experimental program to investigate the strain rate effect. All braces have identical dimensions (Figure 2), were manufactured from the same heat of steel, and were produced by Lead Dynamic Engineering Co., Ltd., a joint venture of Nippon Steel & Sumikin Metal Group and Baosteel Group. The plastic segment of the core is 500 mm long and has a rectangular cross section of 10 mm by 56 mm. Q235B
steel made in China was used for the core material, which is a commonly used structural steel with a nominal yield strength of 235 MPa. The mill sheet of the core steel shows that the measured yield and ultimate tension strengths are 295 MPa and 431 MPa, respectively. The elongation ratio is 33.4%. The elastic segments have a cross-sectional area of 1700 mm$^2$, three times that of the core. The restraining steel tube is 680 mm long and 140 mm in outer diameter and is filled with mortar. Polystyrene cushions are placed at both ends of the plastic segments of the core to produce 30 mm gaps between the core and the infill mortar. The steel core inside the restraining tube is wrapped by 5 layers of cloth tapes before the mortar is filled. The average thickness of each layer of the tape is 1.486 mm, resulting in a 7.431 mm thick debonding layer between the core and the restrainer.

All specimens were subjected to cyclic loading in two phases. In the first phase, they were installed in a double-K configuration in reinforced concrete (RC) frame subassemblies (Figure 3) and were subjected to quasi-static loading of increasing amplitudes in the RC frames. The loading histories of all BRBs were similar to each other. Complete details are referenced in Qu et al (2017). After the test, the BRBs were removed from the RC frame subassemblies for the second phase testing, in which they were subjected to uniaxial cyclic loading with constant amplitudes, either dynamic or quasi-static, until the steel cores fractured (see Figure 6). Each BRB was restored to its original length before it was subjected to the second-phase loading.

Table 1 summarized the second-phase loading protocols for the specimens. Three displacement amplitudes of 1.5 $\delta_{\text{em}}$, 2.0 $\delta_{\text{em}}$ and 3.0 $\delta_{\text{em}}$ were chosen, where $\delta_{\text{em}} = 8.45$ mm is the design deformation for the BRBs that corresponds to a 1/100 story drift ratio of a prototype building. For each amplitude, both a quasi-static and a dynamic loading were conducted on separate specimens. For dynamic loading, the loading frequency was set to be 1.0 Hz regardless of the amplitudes, resulting in different maximum strain rates ranging from 9.5%/sec for an amplitude of 1.5 $\delta_{\text{em}}$ to 18.4%/sec for an amplitude of 3.0 $\delta_{\text{em}}$. These strain rates are in the order of 10%/sec, which has been deemed as the maximum strain rate that would usually develop in the seismic response of a structure (Shing and Mahin, 1988).
Table 1. Parameters for second-phase loading of constant amplitudes

<table>
<thead>
<tr>
<th>ID*</th>
<th>Displacement amplitude</th>
<th>Nominal strain amplitude</th>
<th>Loading frequency</th>
<th>Max. strain rate</th>
</tr>
</thead>
<tbody>
<tr>
<td>S1.5</td>
<td>1.5d_bm</td>
<td>0.0254</td>
<td>0.005 Hz</td>
<td>0.0005/sec</td>
</tr>
<tr>
<td>D1.5</td>
<td></td>
<td></td>
<td>1.0 Hz</td>
<td>0.095/sec</td>
</tr>
<tr>
<td>S2.0</td>
<td>2.0d_bm</td>
<td>0.0338</td>
<td>0.004 Hz</td>
<td>0.0006/sec</td>
</tr>
<tr>
<td>D2.0</td>
<td></td>
<td></td>
<td>1.0 Hz</td>
<td>0.111/sec</td>
</tr>
<tr>
<td>S3.0</td>
<td>3.0d_bm</td>
<td>0.0508</td>
<td>0.002 Hz</td>
<td>0.0004/sec</td>
</tr>
<tr>
<td>D3.0</td>
<td></td>
<td></td>
<td>1.0 Hz</td>
<td>0.184/sec</td>
</tr>
</tbody>
</table>

*S in specimen IDs stands for static loading and D for dynamic loading.

The test setup for the second-phase loading is shown in Figure 4. The steel loading jig was driven by a 500 kN actuator along the axis of the specimen to impose uniaxial deformation history. The loading jig was bolted to a pair of sliders, each of which would slide on a linear guideway fixed on the steel base beam. The specimen was bolted to the gusset plates which were connected to the loading jig at one end and the reaction stub at the other end. The gusset connection with high-strength bolts was designed to be slip-critical.

Figure 4. Setup for constant-amplitude uniaxial loading: (a) side view, (b) top view and (c) photo.

The axial deformation of a BRB was measured by a pair of displacement sensors on both sides of the brace (Figure 5). The range of measurement covers the full length of both the plastic and elastic segments of the core as well as a small portion of the enlarged elastic segments for the gusset connection. Considering that the axial stiffness of the elastic segments within the measured range is more than 4 times that of the plastic segment and that the plastic deformation is much larger than the elastic deformation, it is assumed that all deformation is concentrated in the plastic segment. Correspondingly, the nominal strain of the core is approximately taken as the average of the measured axial deformations by the two sensors divided by 500 mm, the length of the plastic segment. In addition, the nominal stress in the core is taken as the recorded axial force divided by 560 mm², the
nominal cross-section area of the plastic segment.

![Displacement sensor](image)

**Figure 5. Measurements**

All specimens were loaded until the core fractured. The histories of nominal strain experienced by each pair of the specimens are compared in Figure 6. In the Phase 2 loading, the strain amplitudes in static loading were close to the target whereas those in dynamic loading were much smaller than the target. This is because, in static loading, the displacement was incremented until the monitored deformation reached the prescribed targets. However, it is impossible for the slow adjustment in a dynamic loading, in which the actuator moved following a displacement time history with prescribed amplitudes. The monitored deformation of the specimen is usually smaller than the actuator movement because the loading setup would also deform. Although larger amplitudes were assigned for the control of the actuator to compensate such a loss in deformation, such a compensation could not be accurate. Also note that the loading history of S2.0 in Phase 1 missed the last loading cycle as compared with the others. This is because the corresponding brace, that is, the lower left one in Specimen No.3 (Figure 4), in the previous subassembly tests was removed in the last loading cycle for the purpose of the previous tests.

![Nominal axial strain](image)

**Figure 6. As-measured strain histories up to fracture of steel cores**

3. Strength deterioration

Figure 7 compares the hysteretic curves of the second-phase loading. All these specimens exhibited full and stable hysteretic curves until the steel core suddenly fractured. Because they already experienced a Phase 1 loading of increasing amplitudes, the specimens did not show much strain hardening in Phase 2 alone. A significant difference in the hysteretic curves is that the dynamically loaded specimens exhibited
significant strength deterioration as compared to their quasi-static loading counterparts. Denote the maximum axial force in the tension and in compression in a single loading cycle as $P_{t\text{max}}$ and $P_{c\text{max}}$, respectively (see Figure 1). The variation of peak forces as the loading proceeds is depicted in Figure 8, in which the cumulative plastic strain, $\Sigma \varepsilon_p$ (Equation 1), is normalized by the yield strain, $\varepsilon_y$, of the steel core and is used as the x-axis.

$$\Sigma \varepsilon_p = 2 \sum (\varepsilon_{t\text{max}} + |\varepsilon_{c\text{max}}| - 2\varepsilon_y)$$

where $\varepsilon_{t\text{max}}$ and $\varepsilon_{c\text{max}}$ is the nominal strain corresponding to the maximum axial displacement in each cycle; $\varepsilon_y = 0.00144$ is the yield strain of the cores.

![Figure 7. Hysteretic curves of Phase 2 loading of (a) 1.5$D_{\text{bm}}$ amplitude; (b) 2.0$D_{\text{bm}}$ amplitude and (c) 3.0$D_{\text{bm}}$ amplitude.](image)

![Figure 8. Variation of peak forces in loading process.](image)

The maximum tension forces of the three quasi-statically loaded specimens were almost identical and decreased very slightly throughout the loading process. For dynamically loaded specimens, however, the maximum tension forces decreased significantly. This is especially obvious for D1.5 and D2.0, the maximum
tension force of which were decreased by 21.5% and 14.6%, respectively, before fracture. As compared to the tension forces, the maximum compression forces were larger, more dispersed and exhibited greater deterioration. The maximum compression forces of D1.5 and D2.0 were decreased by 26.0% and 20.1%, respectively, before fracture. During the test of specimens D1.5 and D2.0, the loading was once suspended because the oil source of the actuator ran out. When the loading was resumed, there was a sudden increase of force, which, again, deteriorated quickly.

The hysteretic curves of the first loading cycle in Phase 2 testing of the specimens are compared in Figure 9. Although the dynamically loaded braces exhibited significantly higher yield stress in the first loading in compression, the difference between the maximum tension force in the first cycle of loading is negligible except for Specimen D1.5.

![Hysteretic curves of first loading cycles in Phase 2 testing of (a) S1.5 and D1.5; (b) S2.0 and D2.0 and (c) S3.0 and D3.0.](image)

**Figure 9.** Hysteretic curves of first loading cycles in Phase 2 testing of (a) S1.5 and D1.5; (b) S2.0 and D2.0 and (c) S3.0 and D3.0.

### 4. Compression over-strength

The compression over-strength factor, or also known as the compression strength adjustment factor, $\beta$, is defined as the ratio of the maximum compression force to maximum tension force in a loading cycle (Equation 2).

$$\beta = \frac{P_{\text{max}}}{P_{\text{tmax}}}$$  \hspace{1cm} (2)

The variation of $\beta$ in each cycles of the second-phase loading is depicted in Figure 10(a). The $\beta$-factor was generally larger in the dynamic test than in the static test. It even exceeded the limit of 1.3 for D2.0 and D3.0 specimens. As the loading proceeded, the $\beta$-factor in the dynamic test was gradually decreased because the maximum compression force deteriorated faster than the maximum tension force. In contrast, the $\beta$-factor in the static test remained constant or even increased as the loading proceeded.

![Relationship of compression over-strength factor and (a) normalized cumulative plastic strain in current second-phase test and (b) average peak strain.](image)

**Figure 10.** Relationship of compression over-strength factor and (a) normalized cumulative plastic strain in current second-phase test and (b) average peak strain.
The $\beta$-factor in each cycle of the Phase 2 loading is plotted against the strain amplitude, $\varepsilon_m = (\varepsilon_{tmax} - \varepsilon_{cmax})/2$, in Figure 10(b). The $\beta$-factors extracted from the published results of quasi-static loading test on BRBs of Q235B steel (Gao et al 2008, 2010, Chen et al 2009, Huang et al 2010, Wang and Gao 2012) and of SN490B steel (Wu et al 2014) are also plotted for comparisons. The $\beta$-factors are generally larger for larger strain amplitudes. An empirical equation for the upper bound of $\beta$-factors, denoted as $\beta_u$, was proposed in a previous study by the authors (Equation 3) to provide an envelope for the test data plotted in Figure 10(b). The $\beta$-factors of the quasi-static loading tests in the current experiment conform well with the empirical upper bound whereas the upper bound is much exceeded by those of the dynamic tests. The dynamic $\beta$-factors remain below the AISC limits at small strain amplitudes, although they are larger than the static counterparts. At larger strain amplitudes, however, the increased $\beta$-factors because of the rate effect may exceed the limits, thus representing a source of non-conservativeness of the current practice of BRB qualification that uses quasi-static cyclic tests. Subassemblage- or system-level investigation is necessary to evaluate the influence of this effect on the components that connect to the BRBs.

$$\beta_u = 1 + \sqrt{2\varepsilon_m}$$  \hspace{1cm} (3)

5. Profiles of buckled steel cores

The restraining tubes were opened after the tests and the profiles of the steel cores were examined (Figure 11). For all specimens, the locations of fracture are away from the transition part between the plastic and the elastic segments, suggesting no stress concentration there. Significant local buckling is observed in the cores of most specimens. The comparison of deflections of the cores show no clear correlation between strain rates and maximum deflections. However, the shapes of the profiles show that the local buckling of the dynamically loaded specimens is more concentrated (as marked by the dashed circles in the graphs) than their quasi-statically loaded counterparts.

Figure 11. Core profile after Phase 2 loading of (a) $1.5A_{bm}$; (b) $2.0A_{bm}$ and (c) $3.0A_{bm}$ amplitudes.
As already mentioned, the compression over-strength of BRBs is attributed to the increased interaction between the core and the restrainer through a layer of un-bonding material. The strain rate effect on the compression over-strength may be partly attributed to the rate-dependent behavior of the un-bonding material.

6. Cumulative deformation capacity

Takeuchi et al (2008) proposed a unified model of evaluating the cumulative deformation capacity of BRBs subjected to various loading paths, in which the hysteretic curves of the specimens need first to be decomposed into three parts: the skeleton curve, Bauschinger part and elastic unloading part by the method introduced in Jiao et al (2011). The skeleton curve is obtained by connecting parts of the load-deformation curves sequentially when the load on the specimen exceeds the previously attained load in either positive or negative direction of loading, and the rest of the curve during loading is taken as the Bauschinger part (Figure 12). The decomposed hysteretic curves of the specimens in the current test are depicted in Figure 13. In the decomposition, the loading of Phase 1 and 2 is taken as a continuous process to respect the path-dependent nature of the decomposition.

Figure 12. Decomposition of a hysteretic curve (adapted from Jiao et al 2008).

In Takeuchi et al (2008)’s model, the cumulative plastic strain at fracture, \( \Sigma \varepsilon_p' \), is estimated by Equation (4).

\[
\sum \varepsilon_p' = \frac{1}{\alpha_S \frac{\alpha_S}{35} + \left(1 - \alpha_S \right)} \left( \varepsilon_{m,ave}^{0.4 + 117.14} \right)
\]  

where \( \alpha_S = \frac{\Sigma \varepsilon_S}{\Sigma \varepsilon_p} \) is the ratio of cumulative plastic strain in the skeleton part and the total cumulative plastic strain; \( \varepsilon_{m,ave} \) is the average strain amplitude.

The experimental cumulative plastic strains, \( \Sigma \varepsilon_p \) (Equation 1), and the decomposed skeleton parts, \( \Sigma \varepsilon_S \), and the Bauschinger parts, \( \Sigma \varepsilon_B \), in the test and those estimated by Equation (4) are tabulated in Table 2, in which \( \Sigma \varepsilon_S \) and \( \Sigma \varepsilon_B \) are obtained by the aforementioned decomposition in Figure 13. There is a general trend for \( \Sigma \varepsilon_p \) to decrease with the increase of \( \alpha_S \). This agrees well with the rate-independent empirical model in Equation 4 (Figure 14(a)). The relative errors of the estimated \( \Sigma \varepsilon_p' \) range from -16.8% to 8.3% for the quasi-statically loaded specimens, and from -24.3% to 3.0% for the dynamically loaded ones. These errors are on the same order as, or even smaller than, those in the test results that were used to calibrate the empirical model (Figure 14(b)).

<table>
<thead>
<tr>
<th>Table 2. Experimental and estimated cumulative deformation capacities</th>
</tr>
</thead>
<tbody>
<tr>
<td>ID</td>
</tr>
<tr>
<td>------------------</td>
</tr>
<tr>
<td>Phase 1</td>
</tr>
<tr>
<td>Skeleton</td>
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<td>Bauschinger</td>
</tr>
<tr>
<td>Overall</td>
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<tr>
<td>Phase 2</td>
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<tr>
<td>Skeleton</td>
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<tr>
<td>Bauschinger</td>
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<tr>
<td>Overall</td>
</tr>
<tr>
<td>Total</td>
</tr>
<tr>
<td>Skeleton</td>
</tr>
<tr>
<td>Bauschinger</td>
</tr>
<tr>
<td>Overall</td>
</tr>
<tr>
<td>( \alpha_S )</td>
</tr>
<tr>
<td>( \varepsilon_{m,ave} ) (%)</td>
</tr>
<tr>
<td>Estimated ( \Sigma \varepsilon_p' ) (%)</td>
</tr>
<tr>
<td>Relative error (%)</td>
</tr>
</tbody>
</table>
Therefore, the test results in the present study are in support of the empirical model by Takeuchi et al (2008) and contrast with the test results by Carden et al (2004), in which a higher strain rate seemed to significantly decrease the energy dissipation capacity, as mentioned in the Introduction. The BRBs used in Carden et al.'s test is similar to those in the current tests in dimensions, nominal yield strength, core section shapes and maximum strain rate, but they differ significantly in types of core steel and loading protocol (LYP-225 steel and cyclic loading with increasing amplitudes in Carden et al (2004)). In addition, the dynamic loading amplitudes in Carden et al.'s test were also significantly different from the static loading because of the same loading control problem as what we had in our tests. As a result, a direct comparison of the cumulative plastic deformation is not justified.
7. Conclusions

Six geometrically-identical BRBs were subjected to either dynamic or quasi-static uniaxial loading to investigate the effect of strain rate on the hysteretic behavior of BRBs. The strain rates of the brace cores range from 9.5% s$^{-1}$ to 18.4% s$^{-1}$ in the dynamic tests. The following conclusions can be drawn from the test results and observations.

(1) The effect of strain rate on the yield strength of the BRBs in tension in the present tests is negligible. This effect is dependent on the material properties of the steel of the cores and is not unique in a BRB. However, the BRBs in the present tests exhibited significant strength deterioration when they are subjected to dynamic loading. This phenomenon was not observed in the counterpart quasi-static test.

(2) The compression over-strength, which is a unique property of BRBs as a component, is more significant in dynamic loading than in quasi-static loading. As indicated by the profiles of the fractured cores, higher strain rates tend to concentrate the local buckling in a smaller portion of the plastic segment of a core, and thus may result in greater interaction between the core and the restrainer. More comprehensive experimental studies are necessary to investigate the mechanism for the increased compression over-strength and its possible relation with the properties of the unbonding material.

(3) In contrast to the single test result in a previous study, the test results in the present test show that the effect of strain rate on the cumulative deformation capacities of BRBs is negligible. The cumulative plastic strain at fracture can be well estimated by an existing empirical model, which considers the effect of loading path but is independent of strain rate effect.

Considering that the number of tests is still very small, the results and conclusions in the current study should be interpreted with cautions that the possible influences of parameters including, but not limited to, BRB geometries, dimensions, types of steel and debonding materials, remain unknown. Further experimental investigations are essential for a definite conclusion to be made.

Acknowledgements

The authors are grateful to Professor Yu Jiao at Tokyo City University and Professor Xiaodong Ji at Tsinghua University for their helpful discussions in analysing the test data. They are also grateful to the anonymous reviewers for their insightful and detailed comments that helped improve the paper. This work was jointly supported by the Scientific Research Fund of Institute of Engineering Mechanics, China Earthquake Administration (Grant No. 2016A05) and the National Natural Science Foundation of China (Grant No. 51878629). The financial supports are greatly appreciated.
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