Postyield Cyclic Buckling Criteria for Aluminum Shear Panels

Aluminum shear panels can dissipate significant amount of energy through hysteresis provided strength deterioration due to buckling is avoided. A detailed experimental study of the inelastic behavior of the full-scale models of shear panels of 6063-O and 1100-O alloys of aluminum is conducted under slow cyclic loading of increasing displacement levels. The geometric parameters that determine buckling of the shear panels, such as web depth-to-thickness ratio, aspect ratio of panels, and number of panels, were varied among the specimens. Test results were used to predict the onset of buckling with proportionality factor \( f \) in Gerard’s formulation of inelastic buckling. Moreover, a logarithmic relationship between buckling stress and slenderness ratio of the panel was observed to predict experimental data closely. These relations can be further used to determine the geometry of shear panels, which will limit the inelastic web buckling at design shear strains. [DOI: 10.1115/1.2793135]

Keywords: buckling, shear, aluminum, inelastic, cyclic, postyield

Introduction

Shear panels of soft alloys of aluminum can be effectively used as a device to dissipate energy through hysteresis for a number of engineering applications. One such application is in the area of earthquake resistant design of structures where these devices are used as a means to dissipate seismic energy and control the seismic response of the structure. With thick webs of shear panels of aluminum alloys of low yield values, not only the problem of elastic buckling is avoided but the onset of inelastic buckling can be delayed even past the yielding. Postyield buckling of panels seriously limits their energy dissipation potential with severe pinching of hysteretic loops. Therefore, shear panels are to be designed to avoid buckling at operating shear strains for various applications [1,2]. The purpose of this study is to experimentally investigate the buckling behavior of aluminum shear panels of low slenderness ratio which buckle after yielding and to develop a criterion for postyield shear buckling of such shear panels.

Inelastic Shear Buckling Stress

The plastic buckling analysis has been attempted using the classical theories of plasticity, which involved the incremental (or flow) and/or the deformation theory of plasticity [3–6]. The solutions for simple cases of plate problems for uniaxial and biaxial monotonic loading have been derived. Azhari and Bradford [7] employed both deformation and flow theory in the complex finite element method for plastic buckling of plates. However, these analytical studies are too complex and computationally intensive making them difficult to use for design purposes. The objective of this study is to provide simple expressions for cyclic plastic buckling of aluminum shear panels based on experimental investigation.

For stresses beyond the proportional limit, the critical buckling stresses by elastic theory (e.g., Euler theory) give exaggerated values. In order to get satisfactory results, the behavior of the material beyond the proportional limit must be considered. At these higher stresses, the modulus of elasticity, or slope of the stress-strain curve, varies depending on the strain level and can be represented by the tangent modulus of elasticity \( E_t \). Substituting \( E_t \) for Young’s modulus, \( E \) in Euler’s buckling formula, inelastic buckling stress \( \tau_b \) can be given as follows [8]:

\[
\tau_b = \frac{\pi^2 E_t}{\lambda^2}
\]

(1)

where \( \lambda \) is the characteristic slenderness ratio. Assuming that the edges are partially restrained against rotation for a panel of shorter dimension, \( a \), and longer dimension, \( b \), characteristic slenderness ratio can be given as per the following expression [9]:

\[
\lambda = \frac{a}{tw} \sqrt{\frac{1.6}{1 + 0.7(a/b)^2}}
\]

(2)

Clark and Rolf [10] showed that rather than using tangent modulus which varies with stress, Eq. (1) can be conveniently reduced to a linear function of the equivalent slenderness ratio \( \lambda \), as shown below:

\[
\tau_b = B_s - D_s \lambda
\]

(3)

where \( B_s \) and \( D_s \) are the material parameters that depend on the yield shear stress of the material. Sharp and Clark [9] summarized the observed behavior of thin aluminum shear webs of plate girders under monotonic loading, which formed the basis of design provisions of the Aluminum Association [11]. However, this relation does not provide good predictions of inelastic buckling stress in the strain-hardening region.

Gerard [12] extended the concept of use of secant modulus (in place of tangent modulus) in determining critical shear stresses above the proportional limit and formulated the plastic web buckling problem as follows:

\[
\tau_b = \eta(t) \tau_E
\]

(4)

where \( \eta(t) \) is a plastic-reduction factor, which is related to postelastic behavior of the plate, and \( \tau_E \) is the elastic buckling stress. Gerard proposed an empirical equation for \( \eta \) as a function of the ratio of shear secant modulus \( G_s \) and shear modulus \( G \) of the shear panel, i.e.,

\[
\eta = f \left( \frac{G_s}{G} \right)
\]

(5)

where \( f \) is a proportionality constant to be determined from experimental data. These relations were developed for monotonic loading; however, they can be extended for reversed cyclic loading. Secant shear modulus \( G_s \) is now defined as \( G_s = \tau_b / \gamma_s \), where
The energy dissipation capacity of aluminum shear panels depends on the mechanical properties of the material to a great extent. A highly ductile material is needed to meet the large inelastic strain demand required in these applications. Soft alloys of aluminum are less susceptible to web buckling problems because of their low yield strength, which enables the usage of thicker webs for the same strength. Widely available Alloys 6063 and 1100 of aluminum for structural applications were used for fabrication of I-shaped specimens with transverse stiffeners. This alloy was chosen for its availability in flat sections of required thickness. This material was not commercially available in the fully soft annealed condition. Instead, a more common T6 temper of 6063 alloy, which is solution heat treated and then artificially aged, was obtained and annealed in the furnace. This annealing process is believed to eliminate the history of prior straining above a reference temperature (such as welding) and stress relieves in the test specimens [15]. Annealing resulted in changing the temper T6 to softer temper O, thus reducing the values of yield stress and ultimate stress of the material. The specimens were aged, was obtained and annealed in the furnace. The annealing process is believed to eliminate the history of prior straining above a reference temperature (such as welding) and stress relieves in the test specimens [15]. Annealing resulted in changing the temper T6 to softer temper O, thus reducing the values of yield stress and ultimate stress of the material. The specimens were raised to a temperature of 413°C and kept at that temperature for 2 h. Then, they were allowed to cool gradually at a rate of 28°C per hour in the heat treating oven. However, no attempt was made to assess the residual stress and its distribution in the specimens before and after the annealing process in the present study. Figure 2 shows the stress-strain behavior of unannealed and annealed aluminum alloys used in the present study. The proof stress for unannealed temper T6 corresponding to 0.2% of strain was 240 MPa, which was reduced to 35 MPa after annealing. The stress-strain curve unannealed tensile coupon tests result in a curve with a sharp knee in contrast to more rounded with much lower yield stress in the case of annealed coupons. Also, elongation of the coupons was increased from around 15% to 30% after annealing (Table 1). In addition to reduction in the yield stress, effect of strain hardening of the material was more pronounced due to annealing.

I-sections of specimens were fabricated mainly using three aluminum strips—two separate strips for each of the flanges and one strip for the web. The flanges were welded to the web from the inside face of the flange using tungsten inert gas (TIG) welding process [16]. Transverse stiffeners were employed in specimens to delay the initiation of the web buckling and were rigid enough so that inclined waves of the buckled plate do not run across the stiffener. To maintain postbuckling capacities of shear panels, each transverse stiffener is proportioned to avoid web buckling with the stiffener and must remain effective even after the web buckles to support the tension field as well as to prevent the tendency of flange to move toward each other. The stiffeners were groove welded to both flanges as well as to the web (Fig. 3).

Nineteen specimens of panels (Specimen 1 as trial specimen) with web thickness of 4.5 mm, 6.5 mm, and 7.6 mm were fabricated with aspect ratios of 0.75, 1.00, and 1.25. For each combination of aspect ratio and web depth-to-thickness ratio, two-paneled as well as three-paneled specimens were fabricated using transverse stiffeners. The clear depth of web and width of flange of all specimens were 152.4 mm and 100 mm, respectively (Fig. 3). Similarly, the thickness of flange was 6.5 mm for specimens with a web thickness of 4.5 mm and was increased to 10 mm for specimens with thicker webs. The geometric properties of all test specimens are summarized in Table 2. Since the flat sections of required thickness for Specimens 14–19 were not available in

<table>
<thead>
<tr>
<th>No.</th>
<th>Alloy</th>
<th>Condition</th>
<th>Percentage elongation</th>
<th>Yield stress (MPa)</th>
<th>Ultimate stress (MPa)</th>
</tr>
</thead>
<tbody>
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<td>240</td>
<td>261</td>
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<td>6063-O</td>
<td>Annealed</td>
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<td>85</td>
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<tr>
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<td>Unannealed</td>
<td>16.42</td>
<td>99</td>
<td>112</td>
</tr>
<tr>
<td>4</td>
<td>1100</td>
<td>Annealed</td>
<td>33.32</td>
<td>25</td>
<td>82</td>
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</table>
Alloy 6063, plates of Alloy 1100-O were used. The material used for flanges of test specimens was the same as that of the web. All the test specimens were annealed before being used in the experiment.

Test Setup. A testing system used in the study was designed, as shown in Fig. 4. The load application system consisted of a servo-hydraulic closed loop actuator (MTS manufactured) with a force capacity of \( \pm 500 \text{ kN} \) and a displacement stroke of \( \pm 125 \text{ mm} \). Loading was applied through a controller unit and a function generator that enabled the servocontrolled actuator to produce preprogrammed displacement histories. The lateral shear load was transferred from the actuator to the specimen through an I-shaped steel beam, which moved back and forth with the actuator. The specimen was bolted securely to the bottom flange of the top beam. The bottom flange of the specimen was bolted to the top flange of an I-shaped steel beam at the bottom which was welded to a steel plate, firmly held to the horizontal strong floor of the laboratory. The lateral out-of-plane movement of the top movable beam was restrained by providing side supports with ball bearings on both sides of its web. This arrangement prevented out-of-plane movement, bending or twisting. In order to prevent the movement of the top beam in the vertical plane, roller bearings were provided on the top flange of the top steel beam, as shown in Fig. 4. The setup was so fabricated that the lateral shear load was applied at the mid-depth level of the shear panel.

The instrumentation consisted of transducers, which included a load cell, linear variable differential transformers (LVDTs), and strain gauges. A set of 45 deg-strain rosettes was used to measure the shear strains at the center of the panel, which was also used to determine the initial modulus of shear rigidity \( G \) of the material. The measurement of force in the specimen was accomplished directly via a load cell located in the actuator arm. A pair of LVDTs was diagonally mounted on either face of the specimen to measure the shearing deformation of the web of specimen. An additional LVDT was mounted on the loading beam to measure the horizontal movement of the actuator.

Displacement History. As stated earlier, the objective of this study is to investigate the force-deformation behavior of the shear links under slow cyclic loading. Slow cyclic implies that load or deformation cycles are imposed on a test specimen in a slow, controlled, and predetermined manner, and dynamic effects as well as rate of deformation effects are not considered [17]. Displacement histories consisted of symmetric reversed cycles of increasing displacements in predetermined steps at a frequency of \( 0.01 \text{ Hz} \) in the ramp wave form in displacement controlled regime. Cycles were performed at shear strain levels of \( 0.005, 0.01, 0.02, 0.05, 0.10, 0.15 \), and \( 0.20 \). Shear strain is calculated as the ratio of horizontal shear displacement of the panel to the clear depth of web plate. The push displacement applied by the actuator was taken as positive and the pull displacement was considered as negative. Each displacement cycle was repeated for three times, as shown in Fig. 5. Such loading program is representative of low cycle fatigue caused by short duration events, such as earthquakes, blasts, etc.

Table 2 Geometric properties of all the specimens

<table>
<thead>
<tr>
<th>Sp. No.</th>
<th>Alloy(^a)</th>
<th>( n )</th>
<th>( t_w ) (mm)</th>
<th>( C ) (mm)</th>
<th>( \alpha )</th>
<th>( \beta )</th>
<th>( l_w ) (mm)</th>
<th>( A_w ) (mm(^2))</th>
<th>( t_s ) (mm)</th>
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<tbody>
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<td>23.5</td>
<td>397.0</td>
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<td>248.1</td>
<td>1116.45</td>
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<td>324.3</td>
<td>1459.35</td>
<td>6.5</td>
</tr>
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<td>190.5</td>
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<td>38.1</td>
<td>400.5</td>
<td>1802.25</td>
<td>6.5</td>
</tr>
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<td>368.9</td>
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<td>483.9</td>
<td>2177.55</td>
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</tr>
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</table>

\(^a\)Alloy specification for plates used to fabricate the test specimens, which were annealed before testing.
bending while resisting the bending effects of tributary tension. Specimen having three panels are able to resist the tension field, limited by transverse stiffeners. However, a recent study shows that their role is rather limited compared to what was traditionally believed to be resisted by the flanges and transverse stiffeners, which keep on accentuating with each additional cycle.

12, 14, 16, and 17 had such a configuration that they did not experience buckling up to 0.25 strain levels. Further, Specimens 8, 9, 11, 12, 14, and 17 showed no buckling at all even at strains up to 0.20 or sometimes even completely avoiding web tearing. Specimen 4 having \( \beta = 23.5 \) buckled at 0.2 strain, whereas Specimen 16 with \( \beta = 23.5 \) buckled at 0.2 strain, whereas Specimen 16 with \( \beta = 20 \) did not buckle until tearing of plate. Thus, as web depth-to-thickness ratio is decreased, the tendency of buckling of the panel is delayed to larger strain levels.

**Effect of Aspect Ratio \( \alpha \).** For web depth-to-thickness ratios of 23.5 and 20, some specimens such as Specimens 8, 9, 11, 12, 14, and 17 showed no buckling at all even at strains up to 0.20 or sometimes even completely avoiding web buckling until the tearing of web plate. Specimen 4 having \( \beta = 23.5 \) buckled at 0.1 strain while Specimen 10 having \( \beta = 20 \) did not buckle until tearing of plate.

**Effect of Number of Panels.** In Specimen 5 (three paneled), larger buckling deformation angle (0.15 strain) was noticed as compared to Specimen 2 (0.1 strain) having two panels while all other parameters were the same. Similar behavior was noticed in Specimen 18 \( (\gamma_0 = 0.25) \) and Specimen 15 \( (\gamma_0 = 0.2) \). Thus, it can be stated that three-paneled specimen buckled at large strain level as compared to two-paneled specimen with other parameters remaining the same. Specimen 4 resisted 78.3 kN while corresponding three-paneled Specimen 7 resisted 129.2 kN and similar observation was made in other specimens also. It is observed that the ultimate load level achieved in three-paneled specimens is about 1.5 times the corresponding two-paneled specimens with other parameters remaining constant. This may be due to the tension field developed in the central panel resisted by the adjacent outer panel web, which is not present in the case of two-paneled specimens.

### Criteria for Postyield Shear Buckling

Test results presented in Table 2 can be used to predict the proportionality factor in Gerard’s formulation for the onset of inelastic buckling, as discussed earlier. On plotting the experimental data as shown in Fig. 8, it is clear that the data points lie in a “triangular” banded region with proportionality factor \( f \) ranging from 3.0 to 7.0. This is primarily due to large variations in geometric configuration of shear panels, especially due to two panels versus three panels. It can be observed that the value of \( G_s/G \) of Specimen 18 has been decreased as compared to Specimen 15 due to an increase in the number of panels with other geometric parameters remaining the same. Similar reduction in \( G_s/G \) was observed in Specimen 5 as compared to Specimen 2 due to an increase in the strain level at the onset of buckling resulting in lower value of \( G_s \). Thus, the suggested range of values of \( f \) takes into account the effect of number of panels into consideration as well.

However, for convenience, a best-fit line has been plotted for the dataset, which suggests the value of \( f \) to be 4.92. Using this value, strain at the onset of postyield buckling \( \gamma_0 \) can be obtained as...
Table 3 Aluminum shear panel test results

<table>
<thead>
<tr>
<th>Sp. No.</th>
<th>λ</th>
<th>τ_E (MPa)</th>
<th>τ_b (MPa)</th>
<th>G (MPa)</th>
<th>G_s/G</th>
<th>G_s/E</th>
<th>f</th>
<th>\bar{γ}_b</th>
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<tbody>
<tr>
<td>1</td>
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<td>49.3</td>
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<td>23.220</td>
<td>0.023</td>
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<td>0.008</td>
<td>0.024</td>
<td>3.175</td>
</tr>
<tr>
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<td>2015.5</td>
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<td>3459.3</td>
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<tr>
<td>18</td>
<td>17.2</td>
<td>2338.9</td>
<td>58.9</td>
<td>131.0</td>
<td>32.340</td>
<td>0.004</td>
<td>0.025</td>
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<td>55.0</td>
<td>157.2</td>
<td>28.640</td>
<td>0.005</td>
<td>0.027</td>
<td>4.974</td>
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</tbody>
</table>

*Average value of shear modulus considered because of erratic strain gauge data.

No incident of buckling.
Elastic critical stress is given by [19]

\[ \tau_c = k_s \frac{\pi^2 E}{12(1 - \nu^2)} \left( \frac{1}{\beta} \right)^2 \]  

where \( E \) is Young’s modulus, \( \nu \) is Poisson’s ratio, and \( k_s \) is the buckling coefficient, which depends on aspect ratio of the web subpanel formed by the transverse stiffeners and its boundary restraint conditions. It is reasonable to assume clamped edge conditions for the web panel, as the stiffeners welded to the web and the flanges provide significant restraint to the web. For finite rectangular plate with clamped edges [19],

\[ k_s = \begin{cases} 5.6 + \frac{8.98}{\sigma^2} & \text{for } \alpha \leq 1 \\ 8.98 + \frac{5.6}{\sigma^2} & \text{for } \alpha \gg 1 \end{cases} \]  

Comparing Eq. (7) to Eq. (1) and using the values of \( k_s \) as given in Eq. (8), the slenderness ratio \( \lambda \) can be expressed as follows:

\[ \gamma_b = 4.92 \frac{\tau_c}{G} \]  

(6)

\[ \lambda = \begin{cases} \alpha \beta \sqrt{\frac{1.2}{1 + 0.63 \beta^2}} & \text{for } \alpha \leq 1 \\ \alpha \beta \sqrt{\frac{1.2}{1 + (0.63/\alpha^2)}} & \text{for } \alpha \gg 1 \end{cases} \]  

(9)

Using Eq. (1) for \( \tau_c \) by taking \( E = E \) and value of Poisson’s ratio \( \nu \) as 0.33 in Eq. (6), the web buckling deformation angle \( \gamma_b \) can be expressed as a function of slenderness ratio \( \lambda \) of Eq. (9) as follows:

\[ \gamma_b = \frac{129.17}{\alpha^2} \]  

(10)

Equation (10) can be used to determine the spacing of transverse stiffeners to avoid web buckling by taking equal \( \gamma_b \) to an expected peak-to-peak web deformation angle for fully reversed cycles of loading shown in Fig. 5.

A linear relationship was observed in the log-log plot between slenderness ratio \( \alpha \) of Eq. (9) and ratio of inelastic buckling shear stress \( \tau_s \) to shear yield stress \( \tau_y \), as shown in Fig. 9. Shear yield stress \( \tau_y \) can be defined as 0.6 times of yield stress of material, \( \sigma_{0.2} \) (i.e., stress corresponding to a proof strain of 0.2%). Hence, the shear buckling stress \( \tau_b \) of aluminum panel in the region beyond the yield limit can be expressed in terms of its slenderness ratio \( \lambda \) as follows:

\[ \frac{\tau_s}{\tau_y} = \frac{47.5}{\lambda^{0.87}} \]  

(11)

Shear buckling curve of aluminum panels obtained using Eq. (11) is compared with Euler’s elastic curve, curves proposed by Gerard [12], and the Aluminum Association [11], as shown in Fig. 10. Two buckling curves as per Gerard’s formulation correspond to the minimum and maximum observed values of \( G_s/G \) and 0.15, respectively (Table 3). Gerard’s buckling curve clearly gives the lower bound value of inelastic shear buckling stress. The inelastic buckling curve proposed by the Aluminum Association [11] lies well below the experimental prediction; however, it matched with Euler’s elastic buckling curve at higher slenderness ratio. The proposed postyield buckling curve as given by Eq. (11) lies within Gerard’s buckling band and hence, the prediction of inelastic shear buckling stress for panels of low slenderness ratio is quite reasonable. However, further investigation is needed to justify the validity of the proposed expression in the intermediate region for shear panels of medium slenderness.

Figure 11 shows an array describing \( \alpha \) and \( \beta \) values for panels which buckled at a strain level of 0.15. The region without hatching is the zone in which no buckling took place. Thus, by taking the values of \( \alpha \) and \( \beta \) for shear panels in this zone, the postyield buckling can be completely avoided for the specified strain of 0.15.
Conclusions

This paper presents the basic information on strength and stiffness characteristics, deformation capacities, cyclic strain-hardening effects, and deterioration behavior at large deformations of aluminum shear panels subjected low cycle fatigue, typically associated with extreme events of short duration, such as earthquakes, blast, etc., which are less repetitive at a constant magnitude. The specimens showed very ductile behavior and excellent energy dissipation potential with stable and full hysteretic loops without pinching with shear strains up to 0.20. The deleterious effects of web buckling beyond yield limit can be controlled by reducing the spacing between the transverse stiffeners and thus delaying the onset of web buckling to larger strain levels. As web depth-to-thickness ratio is decreased, the tendency of buckling of the panel is significantly delayed to larger strain levels even until the tearing of web plate.

Experimental study revealed that the proportionality factor \( f \) in Gerard’s formulation varied from 3.0 to 7.0 for shear panels of low slenderness ratio and differing geometries. An expression connecting the web buckling deformation angle \( \gamma_{0w} \) and the web panel aspect ratio \( \alpha \) and the web panel depth-to-thickness ratio \( \beta \) was determined experimentally. It can be used to determine the spacing of transverse stiffeners to avoid web buckling of shear panels. A linear relationship between the ratios of inelastic buckling stress with slenderness ratio of the panel was also established in the log-log plot. A zone of aspect ratio \( \alpha \) and web depth-to-thickness ratio \( \beta \) has been identified in which postyield buckling of aluminum shear panels can be completely avoided.

Acknowledgment

The authors are most grateful to the staff of Structural Engineering Laboratory at IIT Kanpur for their support and help in the fabrication of specimens and testing. The Ministry of Human Resource Development (MHRD) of Government of India, New Delhi, provided funds for this research at IIT Kanpur (Project No. MHRD/CE/20030044), which is gratefully acknowledged.

Nomenclature

- \( A_w \): area of web
- \( B_s, D_s \): material parameters defined by Aluminum Association
- \( a \): shorter dimension of panel
- \( b \): longer dimension of panel
- \( C \): clear spacing of stiffeners
- \( d_w \): clear depth of web
- \( E \): Young’s modulus
- \( E_s \): tangent modulus
- \( f \): proportionality constant as defined in Gerard’s formulation
- \( G \): shear modulus
- \( G_s \): shear secant modulus
- \( k_s \): buckling coefficient
- \( l_w \): length of web
- \( n \): number of panels
- \( t_w \): thickness of web
- \( t_s \): thickness of stiffeners
- \( V \): lateral load
- \( \alpha \): ratio of stiffener spacing to clear depth of web
- \( \beta \): web depth-to-thickness ratio
- \( \Delta \): lateral displacement
- \( \eta \): plastic-reduction factor
- \( \lambda \): characteristic slenderness ratio
- \( \tau \): shear stress
- \( \gamma \): elastic shear strain
- \( \gamma_{0w} \): inelastic cyclic shear strain at buckling
- \( \gamma_{0b} \): inelastic cyclic shear strain at buckling in Gerard’s buckling criterion
- \( \sigma_{0.2} \): proof stress corresponding to 0.2% strain
- \( \tau_{b} \): inelastic buckling stress
- \( \tau_{e} \): elastic buckling stress
- \( \tau_{y} \): yield shear stress

References


