Ductile Web Fracture Initiation in Steel Shear Links
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Abstract: Tests conducted in the 1980s showed that well-detailed short shear links can exhibit stable and ductile cyclic behavior. Recent tests of prevailing A992 rolled shapes revealed that shear links designed according to current seismic specifications can fail by ductile fracture in the link web, a mode of failure that was not observed in earlier tests. This paper investigates the observed ductile fractures through computational structural simulation. An existing criterion for judging the propensity for ductile fracture initiation in steel is modified based on published tests results for notched bars to better pinpoint the location of ductile fracture initiation. Validated finite-element analyses of previously tested shear links are conducted and the results postprocessed to evaluate the potential for ductile fracture of specimens with several different types of details. Reasons for the occurrence of web fractures in new A992 steel beams as opposed to older links are discussed. An alternative stiffener configuration that mitigates ductile fracture and is at the same time practical to construct is proposed.

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CE Database subject headings: Bracing; Frames; Finite element method; Buckling; Fractures; Ductility; Steel.

Introduction
Shear links have been successfully used in eccentrically braced frames (EBFs) for over 20 years (Gálvez 2004). Shear-link-like members have also been used as coupling beams in hybrid coupled walls comprised of reinforced concrete walls connected via steel coupling beams (El-Tawil et al. 2002). Their primary function in both types of systems is to dissipate earthquake energy through large inelastic deformations. Shear links are classified into three categories: short, intermediate, and long, depending on the structural and geometric properties of the links (AISC 2002). When architectural constraints permit, short links which dissipate energy primarily through inelastic shear distortion are preferred to longer links that dissipate energy through plastic hinge rotation. Mechanisms that involve inelastic shear deformation are generally perceived to be more ductile than those involving flexure-related plastic hinge deformations.

Research conducted in the 1980s has shown that well-detailed short shear links exhibit stable and ductile cyclic behavior without brittle failure before reaching a plastic rotation of 0.1 rad (e.g., Hjelmstad and Popov 1983; Malley and Popov 1983). The current detailing requirements and expected rotation angle for shear links in the AISC Seismic Provisions (AISC 2002) are mainly based on test results of links made from ASTM A36 (Fy = 250 MPa) wide-flange shapes as used in the 1980s. In view of the prevailing A992 rolled shapes, testing on shear links made from ASTM A992 steel (Fy = 345 MPa) was conducted at the University of Texas, Austin (Arce 2002; Gálvez 2004) to investigate the adequacy of current requirements for EBF links with higher nominal strength.

As shown in Fig. 1, the tests revealed a new failure mode for shear links, which was not reported in the A36 shear links tested in the past, i.e., ductile web fracture of the link initiating at the top and bottom ends of the stiffener welds. The fracture first occurred at the termination of the fillet welds and then propagated horizontally along the link web. Due to this web fracture, most of the test links could not achieve the target plastic rotation angles required by the 2002 AISC seismic provisions. The unexpected fractures appeared to stem from stress and strain concentrations that occur when stiffener-to-web welds are terminated too close to the k-area (McDaniel et al. 2003). Other factors that are thought to play a role in these failures include: close stiffener spacing (Richards 2004), overstringent loading protocols for EBF links (Richards and Uang 2003), and low fracture toughness in the k-area (Arce et al. 2003; Okazaki et al. 2004).

Several investigators have studied web fracture in shear links using nonlinear finite-element analyses (McDaniel et al. 2003; Dusicka et al. 2004; Richards 2004). The common conclusion from the studies published to date is that the high plastic strain concentration at the termination of welds is responsible for crack initiation. However, plastic strain in itself is not the primary reason that ductile fracture initiates (El-Tawil et al. 1999).

Ductile fracture mechanisms in steel have been studied by various researchers since the late 1960’s. Research shows that ductile fracture initiation occurs in three distinct stages, namely nucleation, growth, and coalescence of microvoids in a plastically deforming metal matrix. Microvoids nucleate at inclusions or second phase particles (carbides and sulfides), either by decohesion/debonding at the particle–matrix interfaces or by fracture of the particles themselves. Void nucleation is followed by a void growth stage where voids grow and interact until localized plastic

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flow and necking of the intervoid matrix occurs. The final phase of ductile fracture occurs when adjacent microvoids coalesce together into a crack. Ductile fracture properties are therefore controlled by the growth and coalescence of voids and ductility depends on the growth phase of microvoids, which is strongly influenced by stress triaxiality, i.e., the presence of high triaxial stresses.

This paper investigates ductile fracture of shear links through computational simulation. First, an existing criterion for judging the propensity for ductile fracture initiation in steel is modified based on published tests results for notched bars. Detailed finite-element analyses of previous tests of shear links are conducted and the results postprocessed to evaluate the potential for ductile fracture of specimens with several different types of details. Reasons for the occurrence of web fractures in A992 steel beams are discussed and an alternative stiffener configuration that mitigates ductile fracture and is at the same time practical to construct is proposed.

Ductile Fracture Initiation Criteria

The most popular ductile fracture initiation models in the literature are based on continuum micromechanics and generally involve study of void growth, interaction, and coalescence under given stress/strain conditions. In these models, it is generally assumed that ductile fracture initiates when microvoids growing within a plastically deforming steel matrix reach some critical percentage of the material volume, i.e., when a specific porosity is reached. Other models have been proposed to characterize steel microstructure, including models based on continuum thermodynamics (e.g., Lemaître 1985) and variational bounds (e.g., Ponte Castaneda and Zaidman 1994).

McClintock (1968) proposed one of the earliest micromechanical void growth models. He theorized that fracture occurs when two neighboring voids come into contact and proposed a failure criterion in terms of critical strain and stress values. He specified that failure occurs when strain and stress quantities over a region of the order of the void size attain critical values. Rice and Tracey (1969) showed that the void growth rate is proportional to the increment of equivalent plastic strain and an exponential function of the triaxial stress state. Fracture initiation criteria based on the Rice and Tracey model have been developed and used by several researchers. In such models, fracture is assumed to occur when the void growth ratio reaches a critical value over the characteristic length of the material, e.g., Rousselier (1987) and Rakin et al. (2004) used the model for monotonic loading applications, while Kanvinde (2004) proposed a modified model for very low cycle fatigue. In other related research, Benzerga et al. (1999) studied the effects of void shape and interparticle spacing on void coalescence using localization-based and plastic limit-load-based models.

Using the assumption of a critical volume fraction of voids, Hancock and MacKenzie (1976) proposed an expression for the failure strain in steel as a function of the triaxial stress state and a material constant. They also recognized the importance of length scale and asserted that it is not sufficient for the failure criterion to be reached at a single point but, rather, the failure criterion must involve a certain minimum amount of material which is a characteristic of the scale of physical events leading to local failure. Their model was used by several researchers including Mackenzie et al. (1977) and Bandstra et al. (1998).

Gurson (1977) proposed a yield criterion and flow rules for a porous (dilatant) ductile, isotropic material by assuming that the material behaves as a smeared continuum. Yielding is governed by a yield surface that exhibits weak hydrostatic stress dependence, while the classical plasticity rules assume that yielding is independent of the hydrostatic stress. Tvergaard (1981) modified the Gurson model by introducing additional parameters which account for interaction of cavities to better fit experimental results for plane strain problems. Tvergaard and Needleman (1984) further modified the Gurson model by introducing an effective porosity parameter which accounts for increasing cavitation after the voids start to coalesce to more closely match experimental observations. The modified Gurson model is implemented in commercial software such as ABAQUS and has been used by Dos Santos and Ruggieri (2003) and Rakin et al. (2004).

Performance Indicators Based on Micromechanics

According to Hancock and Mackenzie (1976), the strain at ductile fracture initiation is

\[ \varepsilon_f = a \exp \left( -1.5 \frac{\sigma_m}{\tilde{\sigma}} \right) = a \exp(-1.5T) \]  

where \( \varepsilon_f \) = failure strain, \( a \) = material constant; and \( \sigma_m \) and \( \tilde{\sigma} \) = hydrostatic and Mises stresses, respectively. The ratio between the hydrostatic and Mises stresses is called stress triaxiality, \( T \). El-Tawil et al. (1999) defined a material independent rupture index (RI) as the ratio between the plastic equivalent strain index and the ductile fracture strain [calculated using Eq. (1)] multiplied by the material constant \( a \), such that

\[ RI = \frac{PEEQ}{\varepsilon_f} = \frac{PEEQ}{a \exp(-1.5T)} \]  

El-Tawil et al. (1999) used RI as an indicator of the propensity for ductile fracture initiation in steel moment resisting connections. The advantage of the index is that it does not include any material constants and so it can be directly applied in parametric studies. Ricles et al. (2000) successfully used the RI proposed by El-Tawil et al. (1999) to develop an optimal access hole geometry for mo-
ment resisting connections, which was subsequently adopted by AISC (2002).

The RI in Eq. (2) was used by the authors to investigate the fracture potential of circular notched bars with varying notch radii tested by Kuwamura and Yamamoto (1997). Kuwamura and Yamamoto (1997) showed that for sharp notches, fracture initiates a small distance under the surface of the notch where triaxiality is maximum whereas for blunt notches, fracture initiates at the center of specimen, again at the location of highest triaxiality. Toribio and Ayaso (2004) also noted that fracture initiates at locations of highest triaxiality. Analysis results show that the RI in Eq. (2) is indeed capable of showing that sharper notches increase the potential for cracking, i.e., RI increases substantially as the radius decreases. However, the index predicts a fracture initiation location that is slightly off from the location observed in tests of sharp-notched specimens. For example, Fig. 2(a) shows that RI achieves its maximum value at the surface of the notch, which is slightly shifted from the correct location just under the surface of the notch.

A modification of Eq. (2) is proposed to remedy the problem identified above: the RI is computed using the maximum triaxiality achieved during the load history rather than the triaxiality computed at a given load step. The modified rupture index (MRI) is therefore

\[
MRI = \frac{PEEQ}{\exp(-1.5 \max[T])}
\]

Experience with MRI shows that in addition to demonstrating that sharper notches increase the potential for fracture, the location of the maximum index coincides more closely with the location of maximum triaxiality, and is close to the experimentally observed fracture initiation location [Fig. 2(b)].

In the computation of the MRI, the plastic equivalent strains and triaxiality values are averaged over a characteristic length of the material which represents the length over which physical processes (void growth and coalescence) are occurring. This is conveniently achieved by using an element size equal to the characteristic length. In this research, the characteristic length is taken as 0.3 mm, which corresponds to the upper limit for A572-Grade 50 steel as reported by Kanvinde (2004).

It is reiterated that MRI is not calibrated to be used as a criterion for fracture initiation; it is only an indicator of the propensity for fracture at a particular location and is used in this respect to distinguish between alternative structural details. An important restriction for using this rupture index is that it is valid for cases where void nucleation strain is small compared to strains over which the voids grow because the void nucleation strain is not included in this model, which is reasonable for structural steel.


The test setup for the shear link tests at the University of Texas, Austin is shown schematically in Fig. 3(a) (Arce 2002 and Gálvez 2004). The setup was devised so that the unequal moments that
develop at each end of a shear link in a D-braced EBF could be reproduced. All of the shear links were comprised of W10×33 wide-flange shapes. The links were tested cyclically under three different loading protocols: severe loading protocol (SEV), AISC loading protocol (AISC), and revised loading protocol (RLP) (Richards and Uang 2003). In general, links subjected to RLP, which was milder than the two other protocols, sustained higher rotation angle before failure while links subjected to SEV exhibited web fracture at relatively lower rotation angles.

A number of specimens were tested, of which seven are chosen for further study in this research. Table 1 shows the measured properties for the materials of which the test specimens were comprised. Table 2 shows the total link rotations (including both elastic and plastic rotations) at which the first crack was observed, as well as the maximum rotation achieved. Since the initiation of cracking was detected visually at the end of a loading cycle, the reported deformations at first cracking may not be precise.

Of the specimens considered further in this work, Links 4A, 4B, and 4C were tested to investigate whether terminating the welds farther away from the k line was beneficial to overall ductility. Link P4 was used to investigate whether web fracture would be exacerbated by welding stiffeners on two sides of the link web instead of using one-sided stiffeners as in 4A, 4B, and 4C. Link P7 had stiffeners that were welded to the link web only to investigate the effect of not welding the stiffeners to the flanges. Link P8 is similar to P4 in that it has two-sided stiffeners. However, the stiffeners were merely welded to the link flanges and not the web. This was done based on the hypothesis that the stress concentration, and therefore web fracture in the vicinity of the k-area, could be eliminated if fillet welding along the link web could be avoided. The link web was sandwiched by the two stiffeners, which constrained out-of-plane web buckling. Link P10 was tested to determine the effect of stiffener spacing on the web fracture. It had only two stiffeners along the length instead of the three that all other links had.

The prototype links used in this research are modeled after the links described above. The prototypes are classified into two groups according to the original naming system used in the test programs: 4-series and P-series (Tables 2 and 3). While prototype links in the 4-series have the same geometry and material properties as the test specimens, the properties of all but P8 of the Prototype P-links are changed slightly so that they match the properties of links in the 4-series. The designation of the affected prototypes links has an appended ‘−M’ to indicate that the properties are modified. The modifications were done to ensure that the parametric studies are focused. For example, the length of the web gap (distance between the inside face of the link flange and the end of the stiffener weld) of specimens in the P-series is increased from 25 to 38 mm so that their results could be directly compared with similar links in the 4-series. In addition, the material properties of the P-links (except for Link P8) are taken to be identical to those of the 4-series to ensure that differences in material properties do not distort important trends in the parametric studies. Table 3 and Fig. 4 summarize the pertinent properties of the prototype links.

### Finite-Element Model Development

The overall finite-element model used in the study is shown in Fig. 3(b). The model is based on measured dimensions reported in Arce (2002) and Gálvez (2004). The link area of the model is discretized using eight-node reduced integration elements with hourglass control in ABAQUS (HKS 2002). Mesh sensitivity studies showed that four layers in the through thickness direction of flanges and stiffeners and six layers in the web are sufficient to produce accurate results [Fig. 5(a)]. Fillet welds connecting the stiffeners to the link web are also modeled using solid elements. However, welds between stiffeners and link flanges, if any, are modeled by imposing tie constraints. Outside of the shear link, a coarse mesh comprised of a combination of solid and tetrahedral elements is used to model other structural elements in the test setup. The global model contains approximately 80,000 solid elements.

As shown in Fig. 5(b), a submodel with a highly refined mesh is used to conduct in depth studies of behavior within critical

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**Table 1. Material Properties of Shear Link Specimens Tested by Arce (2002) and Gálvez (2004)**

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Section</th>
<th>Group</th>
<th>Flange</th>
<th>Web</th>
<th>k-area</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td></td>
<td>$F_y$ (MPa)</td>
<td>$F_y$ (MPa)</td>
<td>$F_y$ (MPa)</td>
</tr>
<tr>
<td>M1</td>
<td>A</td>
<td>M1</td>
<td>0.15</td>
<td>1.50</td>
<td>TW</td>
</tr>
<tr>
<td>M2</td>
<td>B</td>
<td>M1</td>
<td>0.78</td>
<td>0.28</td>
<td>TW</td>
</tr>
</tbody>
</table>

**Table 2. Loading Protocol, Performance, and Material Properties of Shear Link Specimens Tested by Arce (2002) and Gálvez (2004)**

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Loading protocol</th>
<th>Rotation at first crack (rad)</th>
<th>Maximum rotation (rad)</th>
<th>Material group (Table 1)</th>
</tr>
</thead>
<tbody>
<tr>
<td>4A</td>
<td>AISC</td>
<td>0.07</td>
<td>0.07</td>
<td>M1</td>
</tr>
<tr>
<td>4B</td>
<td>AISC</td>
<td>0.07</td>
<td>0.07</td>
<td>M1</td>
</tr>
<tr>
<td>4C</td>
<td>SEV</td>
<td>0.06</td>
<td>0.06</td>
<td>M2</td>
</tr>
<tr>
<td>P4</td>
<td>SEV</td>
<td>0.05</td>
<td>0.05</td>
<td>M2</td>
</tr>
<tr>
<td>P7</td>
<td>SEV</td>
<td>0.13</td>
<td>0.13</td>
<td>M2</td>
</tr>
<tr>
<td>P10</td>
<td>RLP</td>
<td>0.09</td>
<td>0.13</td>
<td>M2</td>
</tr>
</tbody>
</table>

**Table 3. Geometry and Analysis Results of Analyzed Prototype Shear Links**

<table>
<thead>
<tr>
<th>Specimen</th>
<th>Section</th>
<th>Web gap (mm)</th>
<th>Material group</th>
<th>Rotation imposed (rad)</th>
<th>MRI location</th>
</tr>
</thead>
<tbody>
<tr>
<td>4A</td>
<td>A</td>
<td>38</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>4B</td>
<td>A</td>
<td>57</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>4C</td>
<td>C</td>
<td>80</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>P4-M</td>
<td>B</td>
<td>38</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>P7-M</td>
<td>C</td>
<td>38</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>P8</td>
<td>D</td>
<td>25</td>
<td>M2</td>
<td>0.07</td>
<td>LWB</td>
</tr>
<tr>
<td>P10-M</td>
<td>A</td>
<td>38</td>
<td>M1</td>
<td>0.07</td>
<td>TW</td>
</tr>
<tr>
<td>H</td>
<td>E</td>
<td></td>
<td>M1</td>
<td>0.07</td>
<td>0.12 LWB</td>
</tr>
</tbody>
</table>

*TW=termination of web; and LWB=link web.
regions of the link. These regions are identified from the global model and coincide with areas where structural distress was first observed in the tests. The element size used in the submodel (0.3 mm) matches the characteristic length of steel. As previously discussed, specifying such an element size is convenient because it eliminates the need to postprocess element results so that they are averaged over the characteristic length. The chosen element size translates into 24 layers of solid element through entire link web. Mesh sensitivity studies showed that the chosen element length produced sufficiently accurate results. The size of the submodel is chosen so that boundary effects do not influence critical regions where the MRI and other quantities are computed. The submodel is comprised of approximately 60,000 solid elements.

The model accounts for material nonlinearities through classical metal plasticity theory based on the Von Mises yield criterion, associated flow and the assumption of isotropic hardening. Material properties are specified based on coupon tests conducted by Arce (2002) and Gálvez (2004). Since coupon test data for stiffeners were not reported, stiffener material properties used in the analysis are taken similar to link web material properties. Fig. 6 shows the piecewise stress–strain curves at different locations of the shear links, which correspond to the M1 material properties listed in Table 1. The elastic modulus is 200 GPa and Poisson’s ratio is equal to 0.3. The stress values from the dynamic (loading rate of 0.5–3.2 mm/min) coupon tests are chosen in this study to represent possible strength fluctuation during the link tests. As indicated in Fig. 6, the stress–strain curves vary by locations. The k-area properties are assumed to extend 25 mm beyond the flanges into the web as suggested by Gálvez (2004). The yield and ultimate stresses of E7018 electrodes used in the shielded metal arc welding are based on test data in SAC (1999), while the corresponding strains are assumed using reasonable values. The effect of welding on web and stiffener steel is not accounted for due to lack of information. The stress–strain relations in Fig. 6 are adjusted to account for the true plastic stress–strain response be-

![Fig. 4. Specimen sections and details](image1)

![Fig. 5. Finite-element mesh used in study: (a) full model; (b) submodel](image2)

![Fig. 6. Material stress–strain curves: (a) link flange; (b) link web and stiffener; (c) k-area; and (d) fillet weld](image3)
cause large deformations are anticipated in this study (HKs 2002).

Geometric nonlinearities are accounted for through a large strain, large displacement formulation. Geometric imperfections are also accounted for in the analysis. To determine a suitable imperfections pattern, an Eigen analysis is performed to determine the buckled mode shape of the link when subjected to the applied loads. The appropriate buckled shape is identified by comparing to test results of links with buckled webs observed in the tests. A scaling coefficient of 2.5 mm is chosen to represent the maximum value of geometric imperfections based on suggestions in El-Tawil et al. (1999). After scaling, the Eigen shape is added to the original geometry to create a new geometry with an imperfection pattern.

Contact between the stiffeners and the web is enforced for link P8 where the web is sandwiched between stiffeners that are not welded to the web. It is assumed a gap of 0.64 mm exists between the link web and stiffeners to represent fabrication tolerances. Contact friction and stiffener digging into the web are not modeled, and so the resulting model is not capable of simulating the fracture initiation location reported for Link P8.

Each analysis is performed using monotonic displacement control. The right end of each link is monotonically displaced until a total link rotation of 0.07 rad is achieved. This displacement level coincides with the angle at which initial fracture is observed in several of the test specimens. The models used in this work are loaded in a monotonic manner only and it is assumed that conclusions drawn are qualitatively applicable to cyclic conditions.

The modeling assumptions are validated by comparing the link shear versus link rotation angle envelope curve with the one obtained from the test program in Fig. 7. It is clear from Fig. 7 that there is reasonable comparison between analysis and test results. In particular, the initial stiffness, and ultimate strength are well represented. However, yielding appears to have been delayed in the analysis compared to the test. This may be due to residual stress patterns in the steel shape, which are not modeled in this study.

Discussion of Finite-Element Analysis Results

Detailed finite-element analyses of the prototype links listed in Table 3 and Fig. 4 were conducted to investigate the web fracture problem. Results of the analyses were postprocessed to compute MRI quantities within the submodel (listed in Table 3) and produce plots of various other stress and strain quantities as well as load versus shear distortion relationships. Copious results were generated, but only a small representative subset is reproduced here.

Location of Observed Failures

With the exception of P8, all of the links exhibit high MRI gradients and peak values at the ends of stiffener-to-web welds implying that ductile fracture is most likely to initiate at these locations. This is consistent with the experimentally observed locations for fracture initiation. For example, Fig. 8 shows the distribution of MRI in the submodel of Link 4A at the layer of finite elements where the peak MRI value was computed, while Fig. 9 shows how the peak MRI for Link 4A increases with link rotation. It is clear from Fig. 9 that the MRI keeps increasing monotonically even after the system achieves global yield at a rotation of about 0.01 rad (see Fig. 7).

As the shear link deforms, the stiffeners have the tendency to bend at their free edge and twist at their interface with the link web, which in turn leads to warping at the stiffener cross sections attached to the link web (Fig. 10). The warping forces the welds to pull or push the link web as shown in Fig. 10(a), creating highly localized triaxial constraints. Together with the high local strains that develop, these triaxial constraints create conditions that promote early ductile fracture.

In contrast to links with web-welded stiffeners, Link P8, where the stiffeners are welded to the flanges and not the web, exhibits almost uniformly distributed MRI values across the link web. As previously indicated, since contact friction and stiffener digging into the web are not modeled, the model is not capable of simu-
Effect of Stiffener Configuration

An examination of the web of P7-M at a rotation of 0.05 rad (the failure deformation observed in the test) shows that yielding does not occur in the flanges, k-area, or the three areas directly underneath the stiffeners. The k-area does not yield because its yield strength is substantially higher than the surrounding steel, e.g., see Fig. 6(c) and Table 1. Unyielded steel in the k-area was reported by Gálvez (2004) based on observations of whitewash spalling patterns. The areas underneath the stiffeners do not yield because of the reinforcement effect provided by the stiffeners and their welding. The web gap region, however, is yielded and is subjected to high strains and severe strain gradients. When the stiffeners are not welded to the flanges, the finite-element results show that out-of-plane local distortions occur in the web gap area. Similar distortions were reported by Gálvez (2004).

Compared to 4A, Link P7-M had a higher MRI (0.86 versus 0.78), which indicates that welding the stiffeners to the flanges improves the response somewhat. The improvement in behavior appears to result from a reduction in out-of-plane distortion, which is constrained as a result of attaching the stiffeners to the flange. In the case of P4-M, the added restraint provided by the two-sided stiffeners (as opposed to only one stiffener in 4A) further reduces demands in the web between the ends of the stiffeners and the k-area, thereby improving behavior. This is reflected in the MRI for P4-M, which is smaller than that of 4A—0.68 versus 0.78—signifying that given the same loading history, the performance of links with two-sided stiffeners could be expected to be somewhat better than that for links with one-sided stiffeners. This observation was noted by Gálvez (2004) during his experiments.

Effect of Web Gap

Comparing the MRI values for Links 4A, 4B, and 4C, it can be seen that the index decreases rapidly as the web gap increases. As shown in Table 3, at 0.07 rad total rotation, the MRI is 0.78, 0.66, and 0.38 for 4A, 4B, and 4C, respectively, which implies that increasing the web gap reduces the potential for fracture initiation and improves ductility as observed by Arce (2002). As the web gap increases, the stiffeners are less constrained and have a reduced tendency to twist and warp at the stiffener–web interface. Furthermore, the localized yielding and distortions that occur between the k-area and the edge of the welds decrease as the web gap increases, which lowers strain demand, MRI, and the potential for ductile fracture.

Effect of Stiffener Spacing

Based on the performance of Specimen P10 (Table 2), Gálvez (2004) noted that increased stiffener spacing did not appear to be detrimental to ductility. His conclusions were based on a comparison between Specimen P10, which has M2 material properties, and Specimen 4A with M1 properties. An examination of Table 3 shows that the MRI for Prototype P10-M-M2 is indeed comparable to that for 4A (MRI of 0.78 versus 0.80). To eliminate the effect of variation in material properties, the results of 4A should be compared to P10-M-M1 instead, where the MRI is 1.01. This suggests that given the same material properties and loading conditions, the potential for horizontal web fracture is exacerbated by increasing stiffener spacing.

Effect of k-Area Properties and Difference between Old and New Links

The analyses of Link P10-M using M1 and M2 materials (Table 1) demonstrate the substantial influence of the k-area properties. The only difference between P10-M-M1 and P10-M-M2 is the k-area strength; the yield strength of the k-area in M1 is much stronger than the corresponding value in M2. It is clear from the results that P10-M-M1 is more prone to ductile fracture than P10-M-M2 (MRI is 1.01 versus 0.80). The analysis results suggest that the lower k-area yield strength in P10-M-M2 permits yielding to penetrate farther into the k-area, increasing the size of yielded zone in the web gap, which reduces local demands and the potential for ductile fracture at the ends of the stiffener welds.

Another point implied by the analyses is that when the k-area strength is substantially higher than the web steel strength, the reduced toughness of the k-region will likely not play a key role in the link web fractures. The increased strength of the k-area precludes yielding from occurring in this zone, which reduces ductile fracture initiation demands. In other words, yielding in the web steel shields the k-area from high demands, but in doing so,
the web steel is itself subjected to demands that promote ductile fracture initiation at the weld ends, where the demands are highest.

The analysis results for P10-M-M1 and P10-M-M2 and the previous discussion provide insight into why new links perform differently than older links tested in the 1980s. Prior to the 1990s, structural shapes were mostly straightened by the gag method (Tide, private communication, 2004). This trend has changed in the recent past, and the majority of structural shapes are now straightened through a rotary-straightening process. This process is applied continuously along the length of a wide flange shape, and causes large deformations in the k-area leading to substantial work hardening that is manifested in the form of increased strength and reduced toughness in the k-area. On the other hand, the gag method involves straightening loads that are applied at discrete points, i.e., the change in material properties due to work hardening is localized (Bjorhovde 2000). As a consequence, the k-area yield strength of older steel shapes is likely close to that of the link web, which implies that yielding could penetrate farther into the k-area during link shear deformation which, as previously discussed, reduces demands at the weld–web interface and reduces the potential for ductile fracture in the link web.

Proposed Stiffener Configuration
Since higher k-area strength is inevitable in steel shapes produced using current straightening processes, an alternative to Link P8 that reduces the potential for weld fracture and simultaneously delays web buckling is proposed in Table 3 (configuration H) and shown in Figs. 4(e and k). The alternative configuration is comprised of a single longitudinal stiffener welded to the web, rather than multiple vertical stiffeners. The new stiffener configuration reduces the potential for web fracture by eliminating the presence of weld ends near the k-area and the warping effect introduced by vertical stiffeners. The proposed configuration is also practical to construct. In practice, one end of the horizontal stiffener will be welded to the vertical stiffener between the shear link and the outside beam segment; while the other end will be welded to the column flange with a cope to bypass the shear tab. Finite-element analysis of this configuration shows that it has the potential to perform in a ductile manner during cyclic loading. Web buckling was not observed in the simulation and the maximum MRI achieved at 0.07 rad. total rotation is 0.12, which is comparable to that for Prototype P8, with flange-only-welded vertical stiffeners. In the analysis, the horizontal stiffener is assumed to have the same thickness and width as the vertical stiffeners used in the prototypes. In contrast to links with flange-only-welded stiffeners (such as Link P8), the proposed configuration eliminates the rubbing/digging action that occurs between the web and stiffeners, which was observed by Gálvez (2004) to contribute to eventual web fracture.

Summary and Conclusions
Computational structural simulation was used to investigate web fractures that were observed in recent tests of short shear links. An existing criterion for judging the propensity for ductile fracture initiation in steel is modified based on published tests results for notched bars to more accurately pinpoint the location of ductile fracture initiation. Detailed finite-element analyses of previously tested shear links were conducted and the results postprocessed to evaluate the potential for ductile fracture of specimens with several different types of details. The simulation results confirm key experimental observations and suggest that early ductile fracture at the weld–web interface is promoted by the high triaxial constraints that develop at the weld ends coupled with elevated local strain demands in this region.

The simulations are also used to explain the reasons for occurrence of web fractures in new A992 steel beams as opposed to older links. Analysis results suggest that the higher k-area strength in the new shapes shields this zone from high ductile fracture demands, but in doing so, the web steel is itself subjected to high plastic strains coupled with high stress triaxiality which combine to promote ductile fracture initiation at the weld ends.

Based on observations made in this research, a practical alternative stiffener configuration that makes use of a single horizontal stiffener rather than multiple vertical stiffeners is proposed. Although the simulation results indicate promising performance, full scale testing should be conducted to confirm that the proposed stiffener configuration is appropriate. Additional research is also needed to develop methods for designing the proposed new configuration.

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