Behavior of Transverse Fillet Welds: Parametric and Reliability Analyses

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A total of 102 specimens were tested to assess the influence of various parameters on transverse fillet weld strength and ductility and to evaluate the level of safety being provided currently by North American design standards. Details of the test procedures and results are reported in the companion paper (Ng, Deng, Grondin, and Driver, 2004).

The variables included in the study were the filler metal classification (including classifications both with and without a toughness requirement), fillet weld size and number of passes, electrode manufacturer, steel fabricator, root notch orientation, and test temperature. Five filler metal classifications (E7014, E70T-4, E70T-7, E70T7-K2, and E71T8-K6) were investigated. The first classification is a Shielded Metal Arc Welding (SMAW) filler metal, the second and third are Flux Cored Arc Welding (FCAW) filler metals with no toughness requirement and the last two are FCAW filler metals with a toughness requirement of 27 J (20 ft-lbf) at –29 ºC (–20 ºF). Fillet welds with leg sizes of 6.4 mm (¼ in.) and 12.7 mm (½ in.), with the former deposited in one pass and the latter in three, were tested to determine the effect of weld size and number of passes on fillet weld strength and ductility. Electrodes from two manufacturers (identified herein as EM1 and EM2) were used in the study and the welds were deposited by two steel fabricators (designated SF1 and SF2). To emphasize the differences that might occur among steel fabricators, one fabricator (SF1) used an automated welding track to lay the welds. Although the experimental program included primarily lapped splice specimens, six cruciform specimens were also included to provide an indication of the effect of the more severe root notch orientation on both strength and ductility. Three 6.4 mm lapped splice specimens made with E70T-4 filler metal were tested at –50 °C (–58 °F) to investigate the effect of low temperature on fillet weld strength and ductility. All transverse fillet weld tests were performed in triplicate, reducing the influence of welding anomalies on the interpretation of the results. Ancillary test results from all-weld-metal tension tests and Charpy V-notch impact tests are reported in Ng, Driver, and Grondin (2002), Ng and others (2004), and Grondin, Driver, and Kennedy (2002).

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EFFECT OF VARIABLES ON FILLET WELD BEHAVIOR

General

The test results were analyzed to determine the influences of each variable on the behavior of fillet welds. The results are shown in Figures 1 through 6. In the figures, both the mean values, indicated by a solid dot, and the full range of test results are presented in order to provide information about the effect of the parameter on strength and ductility, as well as the associated variability of the results. The strengths of the fillet welds have been normalized by two methods. Where the stress is denoted by P/A_{throat}, the force is assumed to act on the theoretical throat area based on the mean measured leg dimensions and weld length. Consideration of the theoretical throat area is a reflection of the assumption used in design. Where the stress is denoted by $P/A_{fracture}$, the force is assumed to act over the measured fracture surface area, which generally deviated significantly from 45º. This method accounts for both the weld face reinforcement and root penetration. Advantages and disadvantages of the two methods of normalization are discussed in the companion paper (Ng and others, 2004). Taking account of both normalization methods leads to a general interpretation of the results. As expected, in general the stresses normalized by the theoretical throat area are higher than those normalized by the fracture surface area primarily because the former neglects the weld root penetration and face reinforcement. For determining the influence of each variable on fillet weld ductility, the strain at fracture for each case is compared.

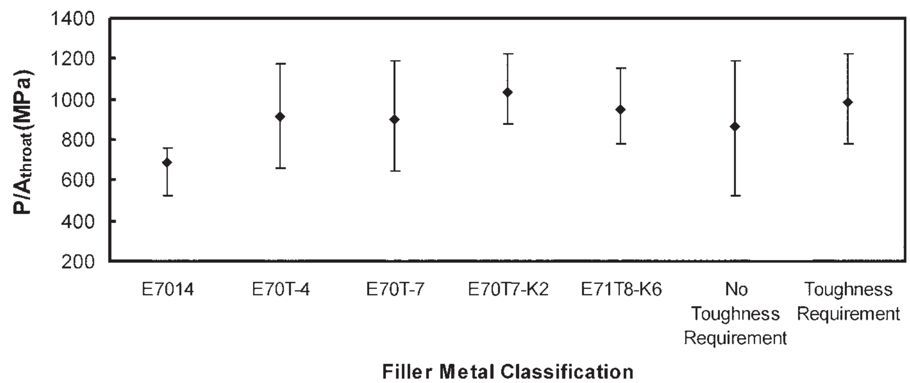
The parameters considered in the analysis, and the total number of individual tests conducted of each type, are:

1. Filler Metal Classification and Toughness—Five different weld electrode classifications were incorporated in the test program, two with a toughness requirement and three without. A total of 75 specimens were fabricated with electrodes without a toughness requirement and 27 were fabricated with electrodes with a toughness requirement.

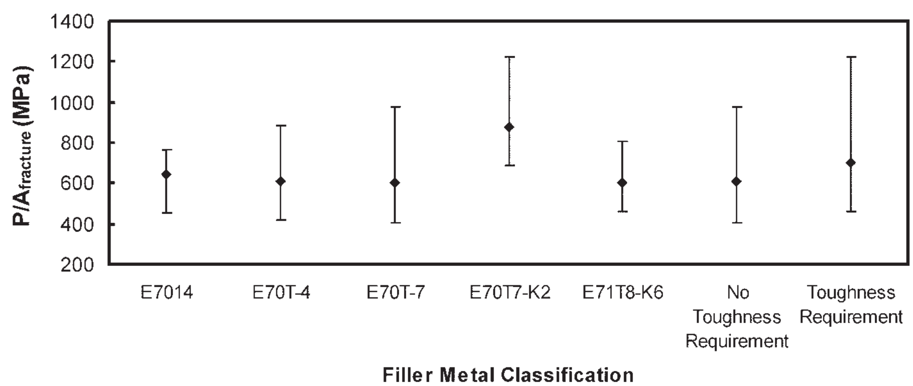
2. Electrode Manufacturer—Electrodes were supplied by two manufacturers. A total of 45 specimens were prepared with electrodes from manufacturer EM1 and 57 from manufacturer EM2.

3. Steel Fabricator—The specimens were fabricated by two steel fabricators. A total of 60 test specimens were fabricated by fabricator SF1 and 42 by fabricator SF2.

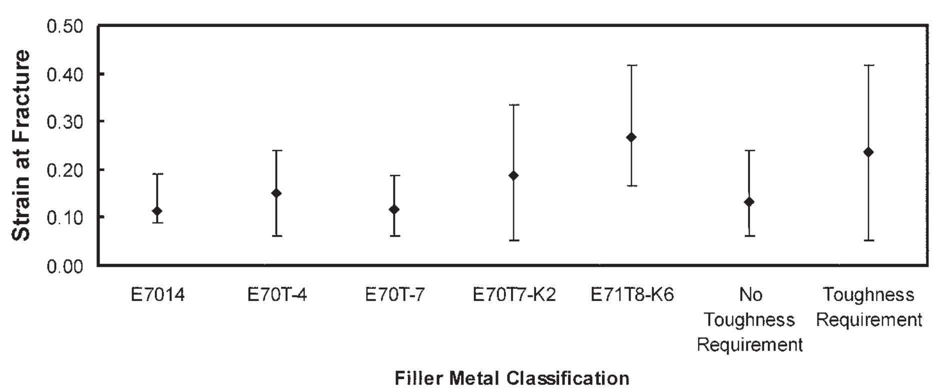
4. Weld Size—Two weld sizes were tested. A total of 63 specimens were fabricated using 6.4 mm welds (one pass) and 39 with 12.7 mm welds (three passes).



(a) Weld Strength Calculated Using Theoretical Throat Area.

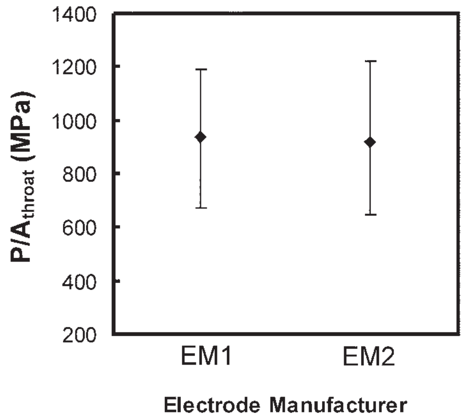


(b) Weld Strength Calculated Using Fracture Surface Area.

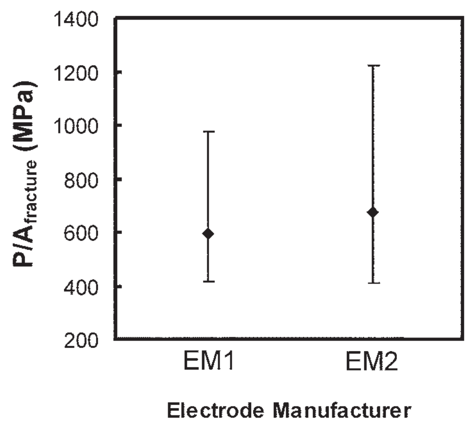


(c) Weld Ductility.

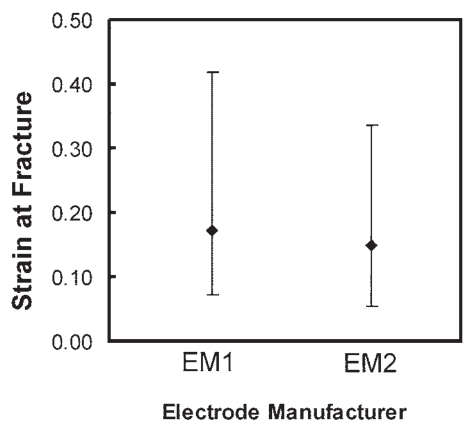
Fig. 1. Effect of Filler Metal Classification on Fillet Weld Behavior.



(a) Weld Strength Calculated Using Theoretical Throat Area.

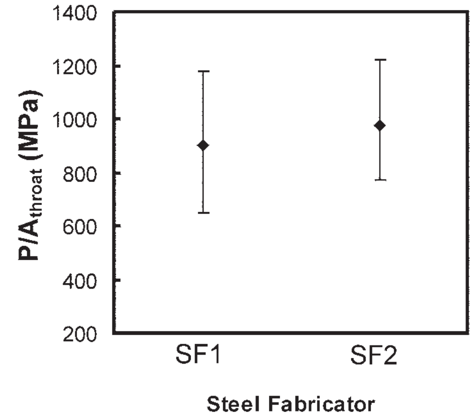


(b) Weld Strength Calculated Using Fracture Surface Area.

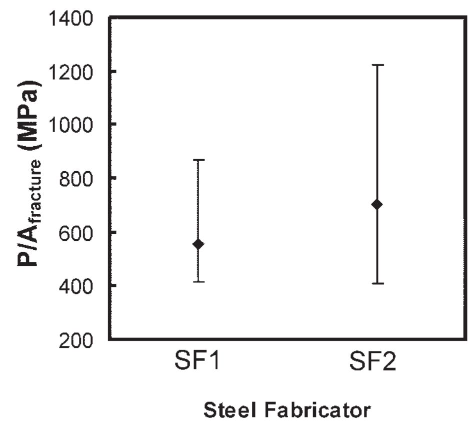


(c) Weld Ductility.

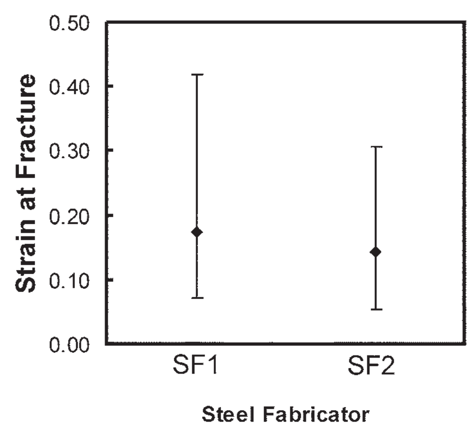
Fig. 2. Effect of Electrode Manufacturer on Fillet Weld Behavior.



(a) Weld Strength Calculated Using Theoretical Throat Area.

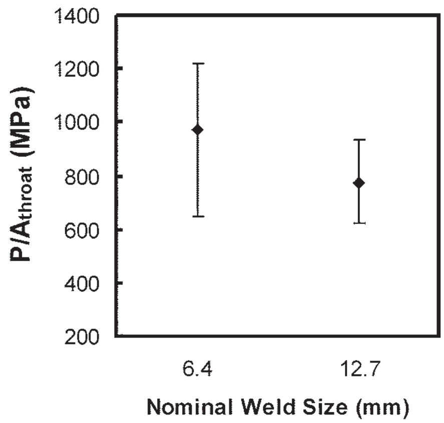


(b) Weld Strength Calculated Using Fracture Surface Area.

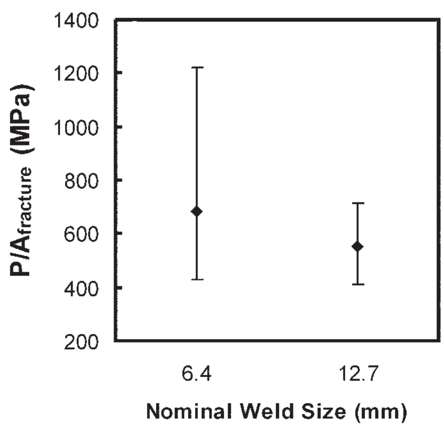


(c) Weld Ductility.

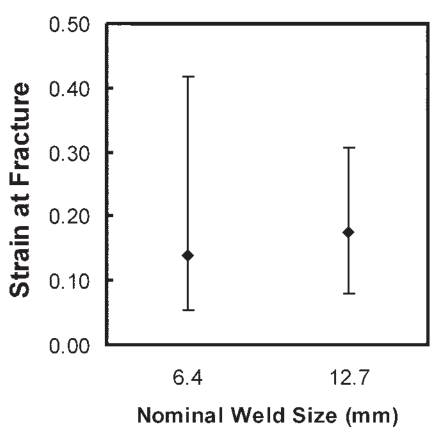
Fig. 3. Effect of Steel Fabricator on Fillet Weld Behavior.



(a) Weld Strength Calculated Using Theoretical Throat Area.

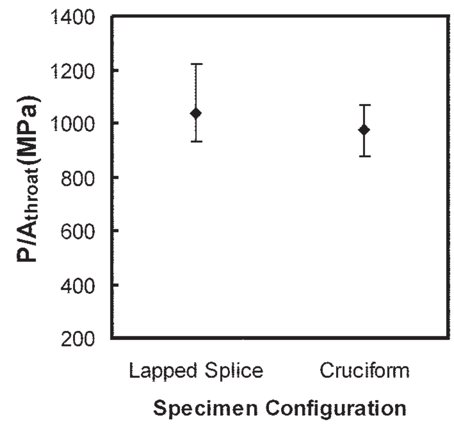


(b) Weld Strength Calculated Using Fracture Surface Area.

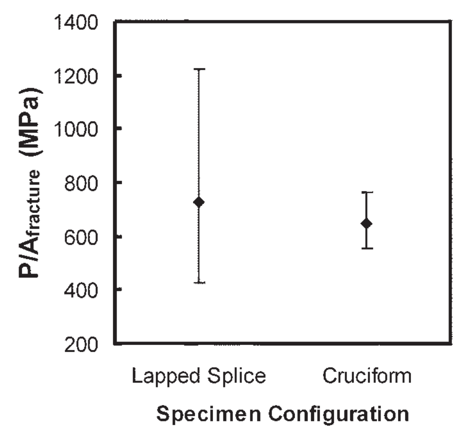


(c) Weld Ductility.

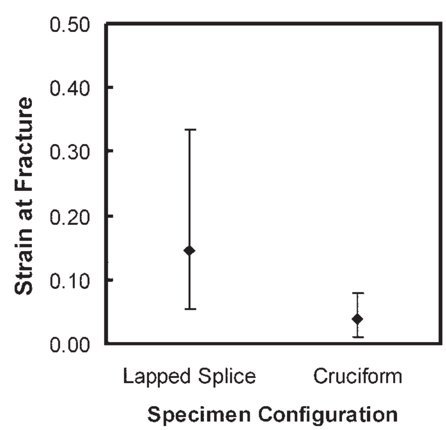
Fig. 4. Effect of Weld Size on Fillet Weld Behavior.



(a) Weld Strength Calculated Using Theoretical Throat Area.

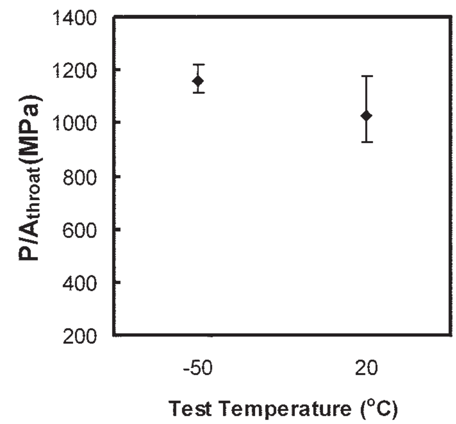


(b) Weld Strength Calculated Using Fracture Surface Area.

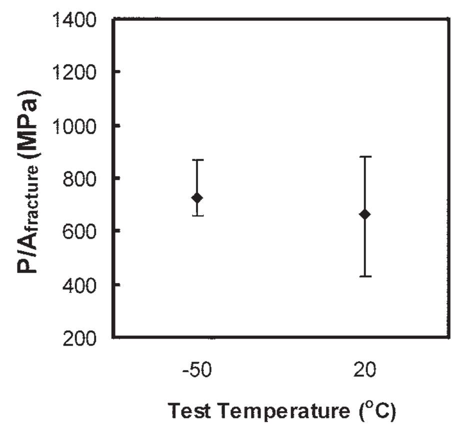


(c) Weld Ductility.

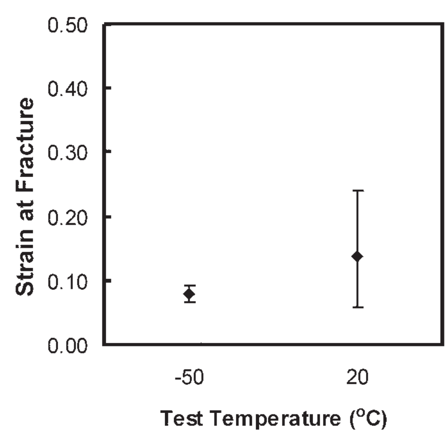
Fig. 5. Effect of Root Notch Orientation on Fillet Weld Behavior.



(a) Weld Strength Calculated Using Theoretical Throat Area.



(b) Weld Strength Calculated Using Fracture Surface Area.



(c) Weld Ductility.

Fig. 6. Effect of Low Temperature on Fillet Weld Behavior.

5. Root Notch Orientation—Two root notch orientations were considered in the research. A total of 96 specimens were fabricated as lapped splices, while six were fabricated in a cruciform configuration.

6. Test temperature—All the specimens were tested at room temperature except for three lapped splice E70T-4 specimens that were tested at –50 °C.

Although not intended to be a primary variable in the research, the effect of the unequal leg sizes is also discussed. The differences in leg size arose from the fact that the specimens were all welded in the horizontal position. The magnitude of the difference varied considerably among the specimens.

Due to the unique nature of the tests on cruciform specimens and on lapped splice specimens tested at –50 ºC (items 5 and 6 above), and the fact that these were manufactured in small numbers without varying all of the other parameters (items 1 to 4), the results for these particular specimens are not included in the comparisons that follow except where they are being specifically investigated. That is, the cruciform specimens and the –50 ºC specimens are excluded from Figures 1 through 4. Other cases where tests have been excluded in the comparisons, and the associated reasons, are discussed in the sections that follow.

Filler Metal Classification and Toughness

The Charpy impact test results (Ng and others, 2004) showed that the toughness of the E7014 filler metal (SMAW) was about three times higher at –29 ºC (–20 ºF) and 21 ºC (70 ºF) than the FCAW filler metals without a specified toughness. The FCAW filler metals with a specified toughness had Charpy impact energies up to about 15 times higher than those without a specified toughness. These significant differences in toughness permit an assessment of the effect of toughness—as well as the other properties of the various electrode classifications utilized—on strength and ductility. The comparison among the different electrode classifications tested, as well as the grouped classifications with and without a toughness requirement, is presented graphically in Figure 1.

Lapped splice test results normalized using the theoretical throat area show that specimens made with E7014 filler metal had the lowest mean strength among all filler metals. It should be noted that for all these SMAW specimens, the fracture surface areas are similar to the theoretical throat areas. Conversely, for the majority of FCAW specimens, the fracture surface areas are in the order of about 1.5 to 2 times larger than the theoretical throat areas, which is largely because of the relatively higher penetration achieved with the FCAW process. Comparing the strength of fillet welds made with SMAW filler metal to those made with

FCAW filler metals based on results normalized using the fracture surface area accounts for the significant difference in penetration. Test results normalized using the fracture surface area show no clear distinction in mean strengths among filler metals without a specified toughness. The results normalized using both methods show that fillet welds made with filler metals with a specified toughness provide higher mean strength than those without, although in the all-weld-metal tension coupon test results they did not show the highest mean strength. Normalized using the fracture surface area, the ratio of the mean strength of specimens made with filler metals with a specified toughness to that of those without a specified toughness is about 1.15. The welding process (SMAW or FCAW) appears to have had little effect on fillet weld strength taken on the fracture surface area, although it may affect the degree of penetration, which would, of course, affect the connection capacity. The method of normalization also appears to have a significant effect on the variability in strength in some cases.

The lapped splice test results show that the specimens made with filler metals with a specified toughness had the highest ductility—as defined by the mean failure strain—among all types of filler metal considered, although in individual cases somewhat lower ductility was also observed. The ratio of the mean fracture strain of specimens made with filler metal with a specified toughness to that of those without is about 1.81. Fillet welds made with filler metal with a specified toughness also showed significantly higher variability in ductility. Of the electrodes without a specified toughness, no obvious distinction in mean ductility was found between fillet welds made with E7014 and E70T-7 filler metals. The mean fracture strain of fillet welds made with E70T-4 filler metal was somewhat higher, with ratios of E70T-4 fracture strains to those for the E7014 and E70T-7 filler metals of about 1.33 and 1.30, respectively. It can be concluded that the toughness of a filler metal has a significant influence on fillet weld ductility.

Electrode Manufacturer

In this comparison, results of fillet welds made with E7014 filler metal were not included because E7014 filler metal from only one electrode manufacturer, EM2, was used in the study. All-weld-metal tension coupon test results (Ng and others, 2004) show that specimens made with equivalent filler metals manufactured by the two electrode manufacturers provided a similar level of mean strength.

A comparison of the effects of the electrode manufacturer on fillet weld behavior is presented graphically in Figure 2. Results of lapped splice specimens normalized using the theoretical throat area show that fillet welds made with filler metals produced by the two electrode manufacturers exhibited similar strength, but results normalized using the fracture surface area show that the ratio of the mean strength of fillet welds made with EM2 filler metals to that of those made with EM1 filler metals is about 1.14. However, the strength range of the EM1 specimens shows less variability and is overlapped entirely by the EM2 specimens, indicating that it is unlikely that any difference in strength should be considered significant.

Lapped splice test results show that the ratio of the mean fracture strain of specimens made with EM2 filler metal to that of those made with EM1 filler metal is about 0.87. Nevertheless, the two ductility ranges observed were large and there is significant overlap. The all-weld-metal tension coupon results also show that the mean ductility was similar.

Steel Fabricator

In this comparison, results of fillet welds made with E7014 filler metal are not included because these specimens were produced by only one steel fabricator, SF1. The results for the lapped splice specimens are summarized graphically in Figure 3. From the normalizations using the theoretical throat and the fracture surface areas, the ratios of the mean strength of SF1 specimens to that of SF2 specimens are about 0.92 and 0.79, respectively. However, when taken on the fracture surface area, the strengths for the fillet welds produced by fabricator SF2 also showed significantly higher variability. It can be concluded that there can be significant variability in weld strengths among fabricators (welders), although it should be emphasized that this is likely influenced greatly by the welding parameters selected.

The ratio of the mean ductility of the SF1 specimens to that of the SF2 specimens is about 1.21. The ductility of the welds produced by SF1 showed significantly higher variability. It can be concluded that significant variability in ductility can be expected among welds produced by different fabricators.

Weld Size

As revealed in many previous studies, fillet weld capacity is not proportional to weld size; smaller fillet welds generally exhibit a higher unit strength than larger fillet welds. A comparison of the two sizes tested in this research is presented graphically in Figure 4. Test results normalized by the theoretical throat area and by the fracture surface area show that the ratios of the mean strength of 6.4 mm fillet welds to that of 12.7 mm fillet welds are 1.26 and 1.24, respectively. From both normalizations, the strength ranges of 12.7 mm fillet welds were at the lower end of the strength ranges of 6.4 mm fillet welds, and considerably less variability was observed for the 12.7 mm welds. It can be concluded that the smaller weld size provides significantly higher unit strength, confirming that fillet weld capacity is not proportional to weld size.

The ratio of the mean ductility of the 6.4 mm specimens to that of the 12.7 mm specimens is about 0.80, although a few of the 6.4 mm welds made with filler metals with a specified toughness exhibited high fracture strains. It can be concluded based on this study that larger welds provide somewhat higher levels of weld ductility with less variability.

Root Notch Orientation

In this comparison, only the results of 6.4 mm fillet welds made with E70T-4 and E70T7-K2 filler metal are included because all cruciform specimens were prepared using these two filler metal classifications and this leg size. A comparison is presented graphically in Figure 5. From normalization using the theoretical throat area, fillet welds in a weldment having a cruciform configuration provide similar strengths to those in a lapped splice configuration. The ratio of the mean strength of lapped splice specimens to that of cruciform specimens is about 1.06. From normalization using the fracture surface area, the ratio of the mean strength of lapped splice specimens to that of cruciform specimens is about 1.13. It can be concluded that the more severe root notch orientation present in cruciform sections may result in slightly lower weld capacity.

The mean ductility of the welds in the lapped splice specimens is about 3.8 times that for cruciform specimens, which showed the lowest ductility of all tests in the experimental program, although the variability in ductility was much greater for the welds in lapped splices. It can be concluded that fillet weld ductility is significantly affected by the root notch orientation, and fillet welds in a cruciform configuration can be expected to provide significantly lower ductility. Because of the relatively small number of cruciform specimens tested, more research is required to study the effect of root notch orientation on strength and ductility.

Test Temperature

In this comparison, only E70T-4 specimens with 6.4 mm fillet welds were included because only specimens of this type were tested at –50 °C. The comparison is presented graphically in Figure 6. The specimens tested at –50 °C showed a higher mean strength than those tested at room temperature. From normalization using the theoretical throat and the fracture surface areas, the ratios of the mean strength of specimens tested at –50 °C to that of specimens tested at room temperature are about 1.13 and 1.09, respectively. It can be concluded that low temperature does not appear to have a detrimental effect on fillet weld strength, although more tests are required to confirm this observation.

The ductility of specimens tested at –50 °C was significantly lower than that of the equivalent specimens tested at room temperature. The ratio of the mean fracture strain of specimens tested at –50 °C to that of the equivalent specimens tested at room temperature is about 0.58. The ductility of the specimens tested at low temperature tended toward the lower end of the observed range of all fillet welds tested in the experimental program. The variability in ductility of the welds tested at –50 °C was very low, although only three tests were conducted, as compared with 18 tests on the same type of specimens tested at room temperature. It can be concluded that, as expected, a very low ambient temperature significantly lowers the ductility of fillet welds.

Unequal Leg Dimension

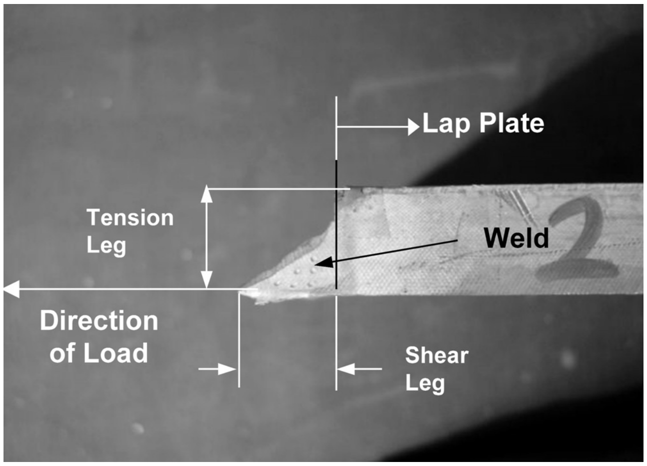
Although the welds were designed to have equal legs, the sizes of the shear leg and the tension leg of the same weld were significantly different in some cases. In these cases, the shear leg was usually larger than the tension leg, which can be attributed to the fact that the specimens were welded in the horizontal position. Test results show that fracture angles were influenced by the ratio of shear/tension leg dimensions, in turn having an effect on the weld strength. In general, when the dimensions of the shear leg and tension leg are about equal, the fracture angle is close to 0°. Welds that failed at an angle close to 90° had a tension leg that was significantly smaller than the shear leg. Figure 7 shows the weld and lap plate from a specimen that fractured at an angle of 0° and another specimen (main plate) with the weld fractured at an angle close to 90°. Details of a fracture surface examination on welds that fractured at various angles, carried out using a scanning electron microscope, are provided in the companion paper (Ng and others, 2004).

Ductility Summary

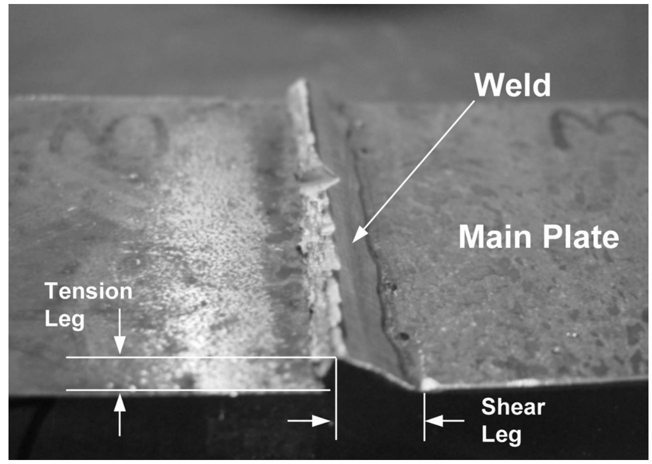
Although the influences of the various parameters investigated on the strength of fillet welds can be interpreted by way of comparison, as presented above, comparisons of ductility perhaps requires additional attention in order to place the values into context. To provide a frame of reference, it is instructive to compare the ductilities observed in this research program with a common benchmark from the literature. As such, Figure 8 shows a comparison of the ultimate and fracture strains of all of the test specimens with the predicted values calculated from the empirical equations presented by Lesik and Kennedy (1990), as derived using regression analyses of the experimental results of Miazga and Kennedy (1989). Lesik and Kennedy obtained the following relationships:

$$\varepsilon_u = 0.209(\theta + 2)^{-0.32} \tag{1}$$

$$\varepsilon_f = 1.087(\theta + 6)^{-0.65} \tag{2}$$

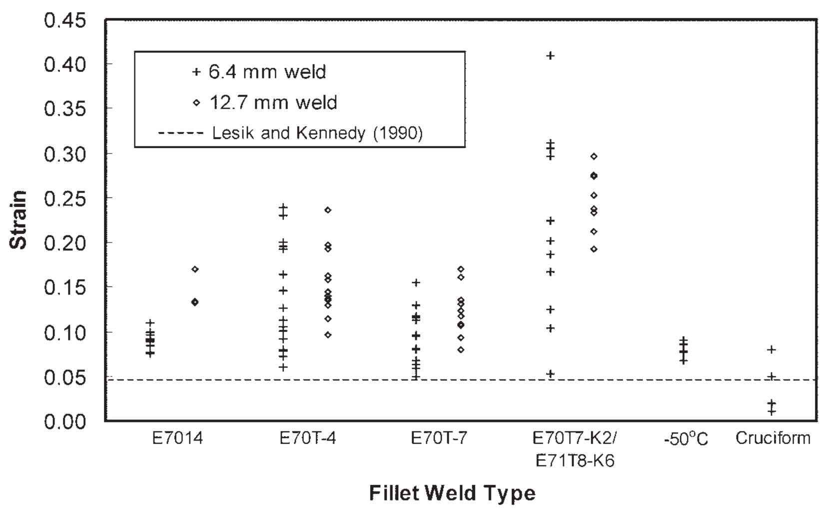


(a) 0° Fracture Angle.

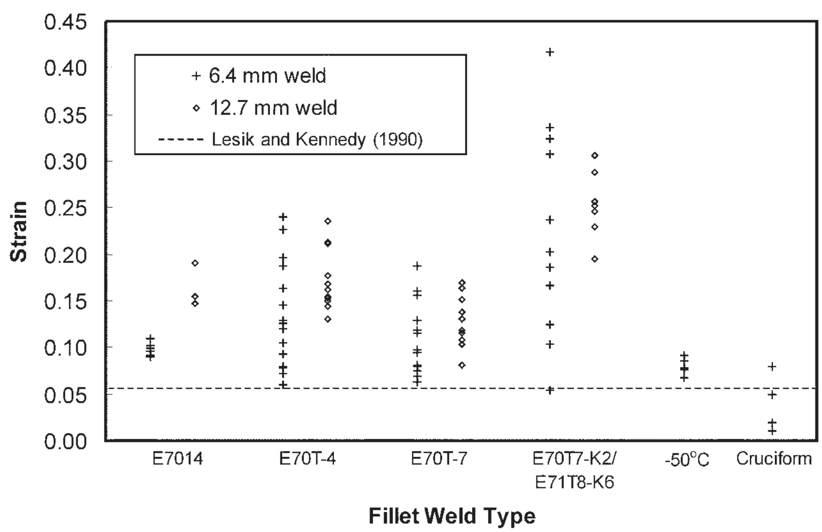


(b) 90° Fracture Angle.

Fig. 7. Typical Weld Fracture Surface.



(a) Fillet Weld Strain at Ultimate Load.



(b) Fillet Weld Strain at Fracture.

Fig. 8. Fillet Weld Ductility Summary.

where ε_u and ε_f are the strain at ultimate stress and the rupture strain, respectively, and θ is the angle, in degrees, of the line of action of the load with respect to the axis of the weld. These equations predict strains for transverse fillet welds at the ultimate load and at fracture of 0.049 and 0.056, respectively, which are shown as dashed lines in Figure 8. In the figure, the specimens tested at –50 ºC and the cruciform specimens have been separated from the other categories due to the unique nature of these particular tests. The great majority of specimens tested in this research exhibited ductilities that exceed these predicted values, and only the cruciform specimens fell consistently below.

EVALUATION OF SAFETY INDEX

The safety index, β, is a common measure for quantifying the level of safety being provided in structures. In the following discussion, it is evaluated for transverse fillet welds designed using either the AISC *LRFD Specification* (AISC, 1999)—Equation 3—or the Canadian standard CSA-S16-01 (CSA, 2001)—Equation 4—based on the results of this experimental program. The procedure is the same as that used in a previous such evaluation by Lesik and Kennedy (1990) using tests on fillet welds produced exclusively using the SMAW process.

$$V_r = 0.60\phi A_W F_{EXX}(1.0 + 0.50\sin^{1.5}\theta) \tag{3}$$

$$V_r = 0.67\phi A_W X_u(1.0 + 0.50\sin^{1.5}\theta) \tag{4}$$

where ϕ is the resistance factor, A_w is the theoretical weld throat area, F_{EXX} and X_u are both the specified minimum tensile strength of the filler metal, and θ is as defined previously. In both equations, the leading coefficient is a factor intended to change the tensile strength of the filler metal to its shear strength and the term in the brackets accounts for the increase in strength observed as the angle between the direction of load and the weld axis increases.

The resistance factor, ϕ, can be calculated as follows [based on Galambos and Ravindra (1978)]:

$$\phi = \Phi_\beta \rho_R \exp(-\beta\alpha_R V_R) \tag{5}$$

A value of 0.55 for the coefficient of separation, α_R, was also proposed by Galambos and Ravindra. Ravindra and Galambos (1978) and Fisher, Galambos, Kulak, and Ravindra (1978) suggested that the safety index, β, be taken as 4.5 for connections to ensure that the probability of failure of the connection is less than that of the member as a whole, for which a value of 3.0 is commonly used in buildings. The quantity Φ_β is an adjustment factor for modifying the resistance factor for cases where β is not equal 3.0, which was assumed in the analysis of the loads. The purpose of the adjustment is described by Fisher and others (1978). An equation for Φ_β, proposed by Franchuk, Driver, and Grondin (2002), has been used in the evaluation of the safety indices that follows:

$$\Phi_\beta = 0.0062\beta^2 - 0.131\beta + 1.338 \tag{6}$$

This equation was calibrated for β in the range of 1.5 to 5.0 and was found to be in error by less than 2 percent for dead-to-live load ratios from 0.5 to 2.0. Although the β values in some cases were found in the current study to be greater than 5.0 (and when $\beta > 3.0$ the error of Equation 6 for a particular dead-to-live load ratio increases with increasing values of β), the maximum error in the value of the adjustment factor presented in the following discussion for dead-to-live load ratios from 0.5 to 2 is about 5.3 percent. This translates into a maximum error in β in the same range of dead-to-live load ratios of 4.5 percent. Modifying Equation 6 could reduce this error somewhat, but a certain degree of potential error is unavoidable when accounting for a broad range of dead-to-live load ratios. In any case, for values of β greater than 5.0, the probability of failure is very small and it is therefore considered that further refinement is unwarranted.

The value of the bias coefficient for resistance, ρ_R, representative of the expected mean-to-nominal resistance, is:

$$\rho_R = \rho_G \rho_{M1} \rho_{M2} \rho_P \tag{7}$$

and the associated coefficient of variation is given by:

$$V_R^{\,2} = V_G^{\,2} + V_{M1}^{\,2} + V_{M2}^{\,2} + V_P^{\,2} \tag{8}$$

where for fillet welds ρ_G is the mean value of the measured-to-nominal ratio of the theoretical weld throat area, and V_G is the associated coefficient of variation. The two material parameters accounting for the variation of material strength are ρ_{M1} and ρ_{M2}. The quantity ρ_{M1} is the mean ratio of the measured ultimate tensile strength of weld metal to the nominal strength. The second material ratio, ρ_{M2}, represents the ability of the coefficient used in the design equation to determine the ultimate shear strength from the tensile strength and is calculated as the mean ratio of measured (failure load divided by the theoretical throat area for longitudinal fillet welds, generally considered to fail in shear on the throat) to predicted ultimate shear strength (0.60 or 0.67, for the AISC *LRFD Specification* and the CSA-S16-01 standard, respectively, times the measured tensile strength from all-weld-metal tension coupons of weld material from the same spool). V_{M1} and V_{M2} are the associated coefficients of variation of these two material parameters, respectively. The quantity ρ_P, the professional factor, is the mean test-to-predicted capacity ratio:

Table 1. Safety Indices

AISC						
	All	6.4 mm	12.7mm	E7014	E70T-4/E70T-7	E70T7-K2/E71T8-K6
Sample Size	86	52	34	12	54	19
No. 6.4 mm	52	52	0	9	31	11
No. 12.7 mm	34	0	34	3	23	8
ρ_G	0.998	0.998	0.998	0.998	0.998	0.998
V_G	0.100	0.100	0.100	0.100	0.100	0.100
ρ_{M1}	1.123	1.123	1.123	1.123	1.123	1.123
V_{M1}	0.077	0.077	0.077	0.077	0.077	0.077
ρ_{M2}	1.248	1.248	1.248	1.248	1.474	1.715
V_{M2}	0.121	0.121	0.121	0.121	0.121	0.121
ρ_P	1.169	1.279	1.001	1.116	1.168	1.208
V_P	0.157	0.111	0.090	0.098	0.168	0.154
ρ_R	1.636	1.790	1.401	1.562	1.930	2.322
V_R	0.235	0.207	0.197	0.200	0.242	0.233
β	4.8	5.7	4.5	5.0	5.5	6.6

CSA-S16-01						
	All	6.4 mm	12.7mm	E7014	E70T-4/E70T-7	E70T7-K2/E71T8-K6
Sample Size	86	52	34	12	54	19
No. 6.4 mm	52	52	0	9	31	11
No. 12.7 mm	34	0	34	3	23	8
ρ_G	0.998	0.998	0.998	0.998	0.998	0.998
V_G	0.100	0.100	0.100	0.100	0.100	0.100
ρ_{M1}	1.123	1.123	1.123	1.123	1.123	1.123
V_{M1}	0.077	0.077	0.077	0.077	0.077	0.077
ρ_{M2}	1.118	1.118	1.118	1.118	1.320	1.536
V_{M2}	0.121	0.121	0.121	0.121	0.121	0.121
ρ_P	1.169	1.279	1.001	1.116	1.168	1.208
V_P	0.157	0.111	0.090	0.098	0.168	0.154
ρ_R	1.465	1.603	1.255	1.399	1.729	2.080
V_R	0.235	0.207	0.197	0.200	0.242	0.233
β	4.8	5.7	4.5	5.0	5.5	6.6

$$\rho_P = Mean\left(\frac{Test \quad Capacity}{A_{throat} \times \tau_u \times \left(1.00 + 0.50\sin^{1.5}\theta\right)}\right) \qquad (9)$$

and V_P is the associated coefficient of variation. Predicted capacity values (the denominator of Equation 9) are calculated using the design equation, with measured values used for the theoretical throat area (based on the measured leg sizes) and the ultimate shear strength. The "measured" ultimate shear strength, τ_u, represented in Equations 3 and 4 by $0.60 \times F_{EXX}$ and $0.67 \times X_u$, respectively, was determined from the results of longitudinal fillet weld tests (Deng, 2003) using welding wire from the same spools as used in the corresponding transverse weld tests.

Table 1 shows the values of the statistical parameters and the resulting safety indices for the variables considered in the research, broken down into six categories. Prior to grouping the specimens into categories, the specimens tested at –50 °C and the cruciform specimens were removed from the data pool since they were targeted investigations and the tests were conducted in small numbers. The "All" category includes the test-to-predicted ratios of all specimens (except those tested at –50 °C and the cruciform specimens) and is intended to reflect the overall level of safety of transverse fillet welds. The remaining categories are for the 6.4 mm and 12.7 mm weld sizes individually and E7014 (SMAW), E70T-4/E70T-7 (FCAW without a specified toughness), and E70T7-K2/E71T8-K6 (FCAW with a specified toughness) filler metals. These latter five categories provide an indication of the relative levels of safety for the various cases of the variables found to be most influential in determining fillet weld capacity. The safety indices for specimens tested at –50 °C and cruciform specimens were not evaluated because the available data are too limited.

The sample for determining ρ_G and V_G for all six categories includes both of the test welds on all specimens reported in Ng and others (2002, 2004), including the specimens tested at –50 °C and the cruciform specimens (in total, 204 samples). This parameter represents only the variability of the weld size; therefore, weldment configuration and test condition are not relevant. The values used for ρ_G and V_G (0.998 and 0.100, respectively) lead to somewhat more conservative values of the safety index than those (1.034 and 0.026, respectively, with a smaller sample size of 42) used by Lesik and Kennedy (1990).

The values of ρ_{M1} and V_{M1} (1.123 and 0.077, respectively) are taken from Lesik and Kennedy (1990) for all categories because the sample size they used to determine this parameter is relatively large (672) as compared to the sample size determined from the all-weld-metal tension coupons tested as part of this research (20). Furthermore, the values of ρ_{M1} and V_{M1} determined from the limited number of tests in this research (weighted equally) would have been similar (1.162 and 0.093, respectively), confirming that the values taken from Lesik and Kennedy (1990) seem reasonable. (It should be noted that the effects of using the lower values of both ρ_{M1} and V_{M1} in the determination of the safety index tend to offset one another.)

The values selected for the parameters ρ_{M2} and V_{M2} for the first four categories shown in Table 1 are those presented by Lesik and Kennedy (1990). Beyond the large sample size (126), these results are also considered to be representative of welds produced using the SMAW process and conservative for FCAW welds. For the two categories that isolate the FCAW process, the values of ρ_{M2} were determined using the FCAW longitudinal fillet weld test data from Deng (2003). These tests were conducted as part of the second phase of this project and the welds were made using the same wire spool as the associated transverse weld specimens. The data of Deng (2003) are considered appropriate for these two categories in that they reflect the increased root penetration often observed in FCAW welds. (These shear strengths were determined using measured weld leg dimensions, but it must be kept in mind that the stresses on the theoretical throat are artificially high since both the penetration and the face reinforcement are neglected. Therefore, higher values may actually reflect the depth of penetration, for example, more than the shear strength of the material itself.) For the E70T-4/E70T-7 category, the data for the two classifications were weighted equally. For the E70T7-K2/E71T8-K6 category, because E70T7-K2 filler metal was not tested by Deng (2003), the data for the E71T8-K6 filler metal were used, which presumes that the ratio of ultimate shear stress to ultimate tensile strength is the same for the two materials (and that the depth of penetration is similar). Note that the differences in the values of ρ_{M2} for the two standards simply reflect the difference in the leading coefficients in Equations 3 and 4. Due to the relatively small number of longitudinal weld specimens presented by Deng (2003) (three each for the three electrode classifications tested), the small values of the coefficient of variation determined from that data are considered to be unreliable. As an approximation of the dispersion, therefore, the value of V_{M2} from the large data set of Lesik and Kennedy (1990) was used in all categories.

For determining ρ_P and V_P, τ_u in Equation 9 for the test specimens made with E70T-4, E70T-7, and E71T8-K6 filler metals is taken from the longitudinal weld test data of Deng (2003). In the cases of the E7014 and E70T7-K2 filler metals, no longitudinal weld specimens were produced. For the E7014 specimens, the results of the longitudinal welds (SMAW) tested by Miazga and Kennedy (1989) were used. For the E70T7-K2 specimens, the results of the longitudinal weld tests for the E71T8-K6 electrodes were multiplied by 1.20, which represents the ratio of the associated ultimate tensile strengths measured from the all-weld-metal tension coupons. The mean values of the ultimate shear strength, τ_u, used in determining ρ_P for the E7014, E70T-4, E70T-7, E70T7-K2, and E71T8-K6 filler metals are 411 MPa (Lesik and Kennedy, 1990), 496 MPa, 545 MPa, 608 MPa, and 506 MPa, respectively.

Although the values of some intermediate parameters differ between the two standards addressed in Table 1, the resulting safety indices are identical. This stems from the fact that the products of the respective resistance factor and shear coefficient are the same, making the predicted factored weld capacity equal for the two standards. Safety indices for all categories are at least equal to the traditional target value for connections of 4.5. (It should be noted that the number of specimens with 12.7 mm fillet welds in every category is fewer than that of specimens with 6.4 mm fillet welds, as shown in Table 1, which could lead to somewhat higher safety indices than if the samples had included randomly selected weld sizes.) The value of the safety index determined from all of the tests is 4.8, which can be considered to be representative of an array of fillet weld conditions, as described previously. (However, it must be recognized that the particular mix of weld sizes and electrode classifications used in this research program is reflected in this value of the safety index.) As expected, the safety index for 6.4 mm fillet welds (5.7) was found to be significantly higher than that for 12.7 mm welds (4.5), with the latter being equal to the traditional target value for connections. Of the categories based on electrode classification, the lowest safety index (5.0) was determined for fillet welds made with E7014 filler metal. Fillet welds made with FCAW filler metals without a specified toughness (E70T-4/E70T-7) resulted in a safety index (5.5) higher than that of SMAW fillet welds, but significantly lower than the safety index (6.6) of fillet welds made with filler metals with a specified toughness (E70T7-K2/E71T8-K6).

SUMMARY AND CONCLUSIONS

Tests on 102 transverse fillet weld specimens were conducted to assess the effect of a variety of parameters on fillet weld strength. Although it was found that the welding process itself (SMAW or FCAW) has little effect on fillet weld strength when normalized on the fracture surface area, it was observed that the FCAW process provides relatively higher root penetration. The majority of FCAW specimens had fracture surface areas in the order of about 1.5 to 2 times larger than the theoretical throat areas. Fillet welds made with filler metals with a specified toughness tended to possess a somewhat higher strength than filler metals without. Furthermore, filler metals with a specified toughness showed significantly higher fillet weld ductility.

Electrode manufacturer and steel fabricator are found to have a small influence on fillet weld strength and ductility. However, these parameters cannot reasonably be incorporated into design equations, and conclusions about their influence on fillet weld behavior cannot be made reliably because of the large variability observed in both cases and the possible interdependence with other parameters.

Smaller fillet welds provided significantly higher mean strength and somewhat lower ductility than larger fillet welds. Smaller fillet welds also exhibited more variability in strength and fracture strain. It is confirmed that fillet weld capacity is not linearly proportional to weld size. Root notch orientation has a slight influence on the strength of fillet welds. Fillet welds in weldments having a cruciform configuration tended to provide a lower strength. They also possessed significantly lower ductility than in the lapped splice configuration. Low ambient temperature was not found to have a detrimental effect on fillet weld strength, but, as expected, the ductility of fillet welds is significantly lower at very low temperatures.

The presence of unequal leg dimensions was found to influence the fracture angle. In general, a 0° fracture angle occurred when the dimensions of the shear and tension leg were similar. A 90° fracture angle appeared when the tension leg was significantly smaller than the shear leg.

The ductilities of all test specimens were compared to the predicted values from the empirical equations presented by Lesik and Kennedy (1990). All lapped splice specimens tested in this research exceeded these predictions. Only the cruciform specimens fell consistently below the predicted values. The low ductility observed for the cruciform specimens could be detrimental for some kinds of joints and further research in this area is urgently needed.

Safety indices were evaluated for transverse fillet welds, based on the results of this test program, using the same procedure as Lesik and Kennedy (1990). The safety indices were determined for six categories: All, 6.4 mm, 12.7 mm, E7014, E70T-4/E70T-7, and E70T7-K2/E71T8-K6. The safety indices for all of the categories are at least equal to the traditional target value of 4.5 for connections.

FUTURE WORK

Including the six cruciform specimens tested as part of this research program, the available data on fillet welds in weldments having a cruciform configuration are still limited. The test results of these six specimens show that transverse fillet welds in cruciform joints tend to provide both lower weld strength and ductility. Experimental work on fillet welds in cruciform joints should be expanded (including different weld sizes) to investigate the effect of weldment geometry (and root notch orientation) on fillet weld behavior in order to determine the safety index for this specific configuration.

For fillet welds tested at low temperature, the available data are also very limited. Although the test results from the present research program did not show that low temperature has a detrimental effect on fillet weld strength, the available data are not sufficient to determine the safety index for this specific category. Further investigation is needed to expand the data pool for fillet welds at low service temperatures.

The observations about fillet weld behavior and the safety indices presented herein were based on the results of tests on isolated transverse fillet welds, and therefore apply only to this condition. Two associated research programs, currently being conducted by the last two authors, consist of investigations into the behavior and reliability of fillet welds with different orientations, and connections with different combinations of fillet weld orientations acting together as a weld group. The latter investigation, when taken together with the results on individual fillet welds, will provide additional insight into the impact of weld ductility on joint behavior.

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