Behavior of Transverse Fillet Welds: Experimental Program

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The behavior of fillet welds has been known for decades to be a function of the direction of the applied load with respect to the weld axis. Experimental studies have shown that fillet welds loaded transversely (perpendicular to the axis of the weld) tend to have the upper bound of strength, but the lower bound for overall connection ductility, and those loaded longitudinally (parallel to the axis of the weld) tend to have the lower bound of strength, but the upper bound for overall connection ductility. Based on the results of tests on full-scale welded specimens, Miazga and Kennedy (1989) proposed a method to predict the strength of fillet welds based on the maximum shear stress failure criterion. Their method was validated by a comparison with their own tests and tests from other researchers (Butler and Kulak, 1971; Clark, 1971; Holtz and Hare, unpublished data). Lesik and Kennedy (1990) extended the work of Miazga and Kennedy and proposed a simplified equation for which the predicted weld strength deviates from that predicted by Miazga and Kennedy by less than 1.5 percent. This equation is a function only of the angle, θ, between the axis of the weld and the line of action of the load and takes the following form:

$$V_\theta / V_0 = 1.0 + 0.50 \sin^{1.5} \theta \tag{1}$$

where V_θ/V_0 is the ratio of the strength of the fillet weld (oriented at an angle θ) to that of an equal size longitudinal fillet weld. This equation has been adopted by both the Canadian structural steel design standard, CSA-S16-01 (CSA, 2001b), and in Appendix J of the American Institute of Steel Construction *Load and Resistance Factor Design Specification for Structural Steel Buildings* (AISC, 1999) hereafter referred to as the *LRFD Specification*. This method predicts that a transverse fillet weld has 50 percent more capacity than an equal size longitudinal fillet weld. The broad applicability of this large increase in strength for transverse welds has recently been questioned since Miazga and Kennedy performed all their tests using one filler metal (E7014) and the shielded metal arc welding (SMAW) process that is used by the steel industry for shop fabrication far less often than other processes such as flux cored arc welding (FCAW). E7014 electrodes have no specified toughness requirement; however, the level of toughness was not determined and may have been influential since SMAW generally produces welds with toughness levels that routinely exceed 27 J (20 ft-lbf) at room temperature. Furthermore, the significance of other parameters on the behavior of fillet welded connections, such as filler metal classification (with and without specified toughness) and manufacturer, steel fabricator, weld size and number of passes, stress level in the base plate, orientation of the root notch, and ambient temperature, still needed to be assessed systematically. An experimental program was therefore initiated by AISC to study these issues.

The main objective of the research presented in the following is to expand the body of experimental results on transverse fillet welds to include the FCAW process and to confirm the applicability of the current provisions of CSA-S16-01 and Appendix J2.4 of the AISC *LRFD Specification* to a broader range of electrode types and other conditions. In particular, the selected filler metals were to possess varying levels of material toughness. The effects of wire manufacturer, steel fabricator, weld size and number of passes, root notch orientation, and ambient temperature were also investigated.

The following presents a review of the literature to obtain a better understanding of the basis for the current design equation and its limitations. A description of the comprehensive test program recently conducted at the University of Alberta to investigate the strength and behavior of transverse fillet welds is then presented. A companion paper (Ng, Driver, and Grondin, 2004) presents parametric and reliability analyses of the test results to evaluate the level of safety being provided by the current design equation.

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LITERATURE REVIEW

A review of the experimental and theoretical studies of the behavior of fillet welds covering from the early 1960s to the present was conducted (Ng, Driver, and Grondin, 2002) and is summarized here. Although the research presented in this paper focuses on transverse fillet welds, a brief review of the work on fillet welds loaded at various angles with respect to their axis is also presented.

An international test series on longitudinal and transverse fillet welds loaded in tension was reported by Ligtenburg (1968). The tests were conducted in ten different countries and involved weld tensile strengths ranging from about 450 MPa (65 ksi) to 580 MPa (84 ksi). The strength of transverse fillet welds was found statistically to be 1.59 times that of equivalent longitudinal fillet welds.

Higgins and Preece (1969) conducted a series of 168 tests on longitudinal and transverse fillet welds. A variety of SMAW electrodes ranging in strength from 415 MPa (60 ksi) to 760 MPa (110 ksi), base metals (ASTM A36, A441 and A514), fillet sizes [6.4 mm (1/4 in.), 9.5 mm (3/8 in.), and 12.7 mm (1/2 in.)], and weld lengths [from 38.1 mm (1 1/2 in.) to 102 mm (4 in.)] were used in this study. Combinations of strong base metal with weaker weld metal and strong weld metal with weaker base metal were investigated. Their test results indicated that there was little effect of weld dilution with the base metal and, therefore, the strength of a welded joint was mainly governed by the strength of the filler metal. For welds made using E70XX electrodes and deposited on a matching base metal (A441), the average strength of transverse fillet welds was 1.57 times that of longitudinal fillet welds.

A series of 23 tests on 6.4 mm fillet weld specimens was conducted by Butler and Kulak (1971) to establish the load versus deformation response of fillet welds loaded in directions varying from 0° (longitudinal) to 90° (transverse) to the weld axis. The test results showed that the increase in strength of the fillet welds was approximately 44 percent as the loading direction changed from 0° to 90°. An empirical equation was obtained to predict the capacity as a function of the direction of the applied load to the weld axis.

Clark (1971) presented the results of 18 fillet weld specimens, all with a nominal leg size of 7.9 mm (5/16 in.). The fillet weld orientations investigated were 0°, 30°, 60° and 90°. The filler metal classification and base metal grade were not reported. The load versus deformation curves of these fillet welds were generally similar to those obtained by Butler and Kulak (1971). The results showed an increase in weld strength of approximately 70 percent as the angle between the direction of the load and the weld axis changed from 0° to 90°. The longitudinal welds also exhibited significantly more deformation at rupture than did the transverse welds.

The effect of weld size on the static strength of transverse fillet welds [on 36 IIW (International Institute of Welding) recommended cruciform specimens] and longitudinal fillet welds (on 36 Werner specimens) was investigated by Pham (1983a, 1983b). The specimens were prepared with FCAW and submerged arc welding (SAW) processes. The FCAW specimens were made with a filler metal containing nickel, resulting in good toughness properties, and the SAW specimens used a general purpose filler metal. Three nominal weld leg sizes were investigated: 6 mm (0.24 in.), 10 mm (0.39 in.), and 16 mm (0.63 in.). The number of weld passes varied from one to three depending on the weld size and welding process. The failure loads indicated that the load capacity was primarily a function of the actual throat size (including weld reinforcement and penetration). For the FCAW specimens, including transverse cruciform and longitudinal Werner specimens, there was a marked reduction in weld strength as the throats increased up to 8 mm (0.31 in.), but there was no further reduction for larger welds. For the SAW specimens the size effect was gradual and extended over the full range of the tested sizes. For longitudinal FCAW specimens, the failure surfaces were along the throat plane. Large longitudinal welds showed better ductility than small welds. This observation was the opposite of the results from cruciform specimens which had transverse welds. A comparison of the results from the cruciform specimens and from the Werner specimens indicates that the transverse welds had a strength from 1.39 to 2.00 times the strength of the longitudinal welds.

Mansell and Yadav (1982) reported the test results of a series of longitudinal and transverse fillet welds using Australian E41 electrodes with a mean tensile strength of 558 MPa (81 ksi). Although the effects of weld orientation and throat size on weld strength were investigated, since in many cases nominal weld dimensions and strengths were used, conclusions about either of these parameters are difficult to draw.

McClellan (1989) performed 96 tests on 6.4 mm and 9.5 mm longitudinal and transverse fillet welds deposited by FCAW. The test specimens were made with high strength low alloy steel plates, commonly used in ship structures. Two FCAW electrodes were investigated, namely, MIL-T71T1-HY and MIL-101-TC/TM. These electrodes have a toughness requirement of 27 J (20 ft-lbf) at –29 °C (–20 °F) and 81 J (60 ft-lbf) at –51 °C (–60 °F), respectively. Weld strength was calculated by dividing the test load by the weld length and the theoretical weld throat determined from weld leg measurements. Fracture surface angles ranged from 42° to 48° for longitudinal fillets and 20° to 25° for transverse fillets. The transverse-to-longitudinal weld strength ratios averaged 1.51 and 1.39 for the MIL-T71T1-HY and the MIL-101-TC/TM electrodes, respectively.

Miazga and Kennedy (1989) tested 42 fillet weld specimens prepared using the SMAW process and E7014 electrodes. Two weld leg sizes with seven loading angles ranging from 0° to 90°, with a 15° increment, were investigated. The ratios of the mean ultimate strength of the transverse welds to that of the longitudinal fillet welds for the 5 mm and 9 mm welds were found to be 1.28 and 1.60, respectively. The higher strength in the transverse welds was attributed to the larger failure surface arising from the smaller fracture angle, and lateral restraint from the base plate, which was designed to remain elastic during the tests. A comparison of the strains of fillet welds loaded at different angles indicated that the ductility of the fillet weld itself is independent of the loading angle. An analytical model based on the maximum shear stress theory was developed to predict the strength and fracture surface angle of fillet welds loaded at various angles. Based on their model, the mean test/predicted ratio for the 42 weld specimens included in their test program was 1.004, with a coefficient of variation of 0.087.

The work of Miazga and Kennedy was extended by Lesik and Kennedy (1990) to consider the strength of fillet welds loaded eccentrically in-plane using the method of instantaneous center of rotation. Equation 1 was derived as a simplified version of the equation proposed earlier by Miazga and Kennedy. The equation predicts an increase in strength of 50 percent as the direction of the load changes from longitudinal to transverse.

A study of the influence of geometrical factors that influence the behavior of fillet welds was presented by Bowman and Quinn (1994). Their experimental program included 18 specimens, including longitudinal and transverse specimens with three leg sizes, 6.4 mm, 9.5 mm, and 12.7 mm, and three different root gaps, varying from 0 mm (ungapped) to 3.2 mm ($^1/_8$ in.). The welds were prepared using E7018, low hydrogen, 4.8 mm ($^3/_{16}$ in.) diameter electrodes. The ratio of strength of transverse/longitudinal fillet welds ranged from 1.3 to 1.7 for the ungapped specimens. For transverse specimens, the larger welds did not demonstrate a significant decrease in unit strength. The 12.7 mm ungapped specimens showed only a 4.5 percent lower unit strength than the measured strength of the 6.4 mm specimens. For the longitudinal specimens, the larger welds did demonstrate a significant decrease in strength (25 percent for the ungapped specimens). The authors argued that the reduction of strength was not strictly due to the increase in leg size; the fact that larger welds tend to have a less convex profile also contributed. Large transverse welds did not show a significant reduction in strength because the transverse welds failed at a smaller angle where the weld reinforcement was small for every size of weld. It was concluded also that the dimple in the exposed weld profile of multi-pass welds contributed to the reduction of strength in longitudinal welds.

Iwankiw (1997) derived a rational basis for the increased fillet weld strength of transverse welds as compared to longitudinal welds. Using the three-dimensional equilibrium of the theoretical throat, and assuming equal weld leg size, Iwankiw derived a general expression relating fillet weld strength to the direction of load. A comparison of his equation with that currently in use in both CSA-S16-01 (CSA, 2001b) and Appendix J2.4 of the AISC *LRFD Specification* (AISC, 1999) indicated that the latter always predicts a higher strength. Iwankiw's equation predicts that the load capacity for a fillet weld loaded transversely is 41 percent higher than one loaded longitudinally.

From this review of the literature it is evident that there exists a large database of test results on transverse and longitudinal fillet welds. The test results indicate that the strength of transverse fillet welds is from about 30 to 70 percent stronger than that of longitudinal fillet welds. The equation used in current North American design standards predicts an increase in strength of 50 percent. Although this increase in strength is well within the range observed in experimental studies, the majority of transverse fillet weld tests were conducted on welds made with the SMAW process, which may have a higher toughness than equivalent welds deposited using FCAW. Only a few recent research programs used fillet welds made using the FCAW process. Furthermore, the effect of weld metal toughness on the strength and ductility of fillet welds has not been evaluated systematically. The behavior of fillet welds at low service temperature was not investigated in any of the previous studies. It is also found that test results for fillet welds with base plates that yield prior to weld fracture are not available and the restraint from an elastic base plate may result in welds with higher strength than those with plates that yield. There is therefore a need to assess the effect of these parameters on the strength and ductility of transverse fillet welds. In particular, tests using filler metals with low toughness characteristics and a more commonly used welding process such as FCAW are needed. In order to expand the pool of data on transverse fillet welds, a comprehensive experimental program was conducted (Ng and others, 2002, 2004; Grondin, Driver, and Kennedy, 2002).

EXPERIMENTAL PROGRAM

The main objective of Phase I of this three-phase research program is to expand substantially the existing experimental data pool on transverse fillet welds to include the FCAW process, and thereby confirm the applicability of the current provisions of North American design standards to a broader range of filler metal types. In particular, the selected filler metals were to possess varying levels of material toughness. Although the experimental program investigates primarily specimens prepared using FCAW, 12 specimens prepared using SMAW with E7014 electrodes were tested in order to

Table 1. Matrix of Test Specimens

(a) Steel Fabricator – SF1

Filler Metal Classification	E70T-4				E70T-7				E71T8-K6		E70T7-K2		E7014	
Electrode Manufacturer	EM1		EM2		EM1		EM2		EM1		EM2		EM2	
Weld Size (mm)	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7
No. of Specimens	12 [a]	3	3	3	3	3	3	3	3	3	6 [a]	3	9	3
No. of Charpy Tests	3 x 2		3 x 2		3 x 2		3 x 2		3 x 2		3 x 2		3 x 2	
No. of Material Tension Tests	5		2		2		2		2		2		2	

[a] Includes three cruciform specimens

(b) Steel Fabricator – SF2

Filler Metal Classification	E70T-4				E70T-7				E71T8-K6		E70T7-K2	
Electrode Manufacturer	EM1		EM2		EM1		EM2		EM1		EM2	
Weld Size (mm)	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7	6.4	12.7
No. of Specimens	3 [a]	3	6	3	3	3	6	3	3	3	3	3
No. of Charpy Tests	3 x 2		–		–		3 x 2		3 x 2		–	
No. of Material Tension Tests	2		–		–		2		2		–	

[a] Tested at -50 °C (-58 °F)

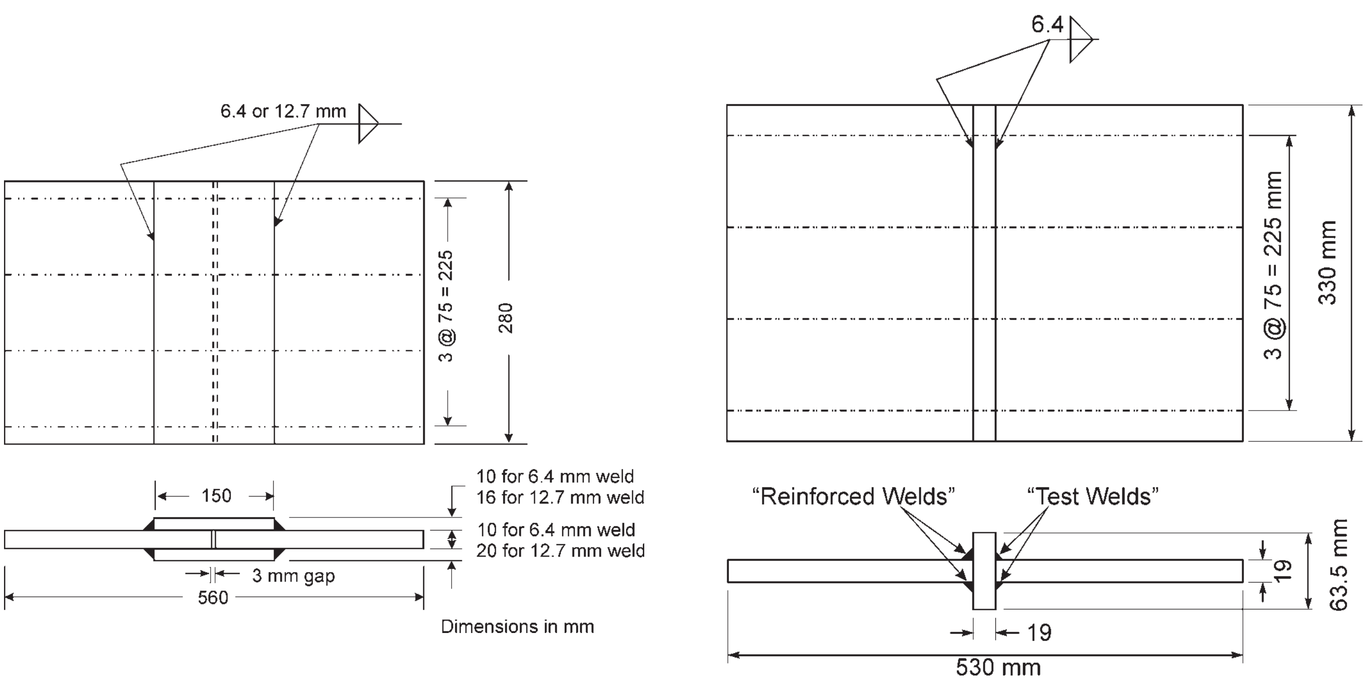
provide a direct comparison with the results of Miazga and Kennedy (1989). Two FCAW filler metals without a specified toughness and two with a specified toughness were investigated. Two weld sizes, 6.4 mm and 12.7 mm, have been included in the study, the former being deposited in one pass and the latter in three. Welds with multiple passes were included, in part, to assess the effect of potential tempering of the root pass by the deposition of subsequent passes. Two local steel fabricators (identified herein as SF1 and SF2) and electrodes from two large manufacturers (identified as EM1 and EM2) were used in the study to assess the associated resulting variability in weld behavior. Six specimens having a cruciform configuration were also tested to assess the need for and the nature of future investigations on the effect of the root notch orientation imposed by the weldment geometry. Three tests were conducted at –50 ºC (–58 ºF) to assess the effect of cold temperature. In all cases, tests were conducted in triplicate to provide a means of assessing statistically the variability of the parameters investigated, as well as their relative importance.

Test Specimens

Five classifications of filler metal were tested, namely, E70T-4, E70T-7, E70T7-K2, E71T8-K6, and E7014 (SMAW), resulting in a total of 102 specimens, as shown in Table 1. Both the E70T-4 and E70T-7 electrodes were obtained from two different wire manufacturers. Since neither E71T8-K6 nor E70T7-K2 electrodes were available from both manufacturers, E71T8-K6 was obtained from manufacturer EM1 and E70T7-K2 was obtained from manufacturer EM2. However, the two electrodes were deemed to be equivalent for the purposes of this study. The first two filler metals (E70T-4 and E70T-7; AWS, 1995) have no specified toughness requirement, while the two other FCAW filler metals (E70T7-K2 and E71T8-K6; AWS, 1998) have a specified toughness requirement of 27 J at –29 °C. The fifth electrode (E7014; AWS, 1991) was included to provide a direct comparison with the tests performed by Miazga and Kennedy (1989). The weld sizes tested were 6.4 mm and 12.7 mm. The test specimens consisted of symmetrical double lapped splice joints, as shown in Figure 1. Fabrication was done in assemblies of three specimens that provided generous run-on and run-off regions in an attempt to provide uniform weld quality within the test specimens. The specimens themselves were then cut from the assemblies and milled to 76 mm (3 in.) width. Plate thicknesses and heats were selected so that the plates yielded prior to fracture of the welds in order to provide the least restraint possible to the weld region near the ultimate strength. All plates were produced to the requirements of both CAN/CSA-G40.21 (CSA, 1999) Grade 350W and ASTM A572 (ASTM, 2000) Grade 50 steel.

All steel plates were supplied by one fabricator, and each filler metal type was from the same spool of wire to ensure that the fabricator would be the only source of variation for cases with the same filler metal classification and manufacturer. Fabricator SF1 used an automated welding track and Fabricator SF2 used a hand-held semi-automatic process to perform the welds in order to accentuate the differences that might occur between two fabricators. Specimens with 6.4 mm welds were welded in a single pass, and specimens with 12.7 mm welds were welded using three passes. All welding was performed in the horizontal position. For visual examination of the root penetration and the extent of fusion, some sections were etched, as shown in Figure 2. In order to reduce both the preparation time and the amount of required instrumentation, two welds from each test specimen were reinforced, as shown in Figure 1, to ensure that failure would take place in one of the other two welds (referred to herein as the test welds).

All-weld-metal tension coupons, Charpy V-notch impact test specimens, and chemical pads were fabricated for all types of filler metal and by both fabricators, except that chemical pads were produced only by SF1. The weld metal tension coupons and Charpy V-notch impact specimens were machined from standard groove welded assemblies fabricated in accordance with ANSI/AWS A5.20 (AWS, 1995) for the FCAW specimens and ANSI/AWS A5.1 (AWS, 1991) for the SMAW specimens. Both the tension and impact specimens were prepared—and the tests carried out—in accordance with ASTM A370 (ASTM, 2002). The chemical pads, prepared in accordance with ANSI/AWS A5.20 (AWS, 1995) and ANSI/AWS A5.1 (AWS, 1991), were tested to confirm that the chemical compositions met



(a) Lapped Splice Specimens *(b) Cruciform*

Fig. 1. Transverse Fillet Weld Test Specimens.

the requirements of the applicable AWS specifications. The ancillary specimens were produced using welding wire from the same spools as for the fillet weld specimens. Two tension specimens were tested for each filler metal, with five specimens (three from one assembly and two from another) being used in one case to assess the repeatability of the results. Two Charpy impact tests were conducted at each of three different temperatures, namely, –29 °C, 21 °C and 100 °C (–20 °F, 70 °F and 212 °F).

Specimen Measurements and Test Procedure

Prior to testing, the sizes of the test welds were measured at eight locations along the weld length. The two legs and the throat dimension were measured using a caliper and adjustable fillet weld gauge, respectively. A more detailed characterization of the weld profile (amount of reinforcement) of the larger 12.7 mm welds was established by means of two additional 45° measurements with the adjustable fillet weld gauge. Figure 3 shows some typical weld profiles both for the 6.4 mm and 12.7 mm welds. In general, it was found that several welds had unequal legs. Either the tension leg was larger than the shear leg, as shown in Figure 3a, or the shear leg was larger than the tension leg, as shown in Figure 3b. The former situation was far less common than the latter. It was also found that several welds had close to equal leg sizes as shown in Figure 3c. This situation was much more common in the 12.7 mm welds than in the 6.4 mm welds.

The test set-up is shown in Figure 4. Four linear variable differential transformers (LVDTs)—two on each test weld—were used to measure weld deformations. The LVDTs were mounted with custom-made brackets to measure only the deformation within the leg dimension of the weld and thereby avoid capturing a significant amount of the deformation of the base plate. This was an important test design consideration because the plates were designed to yield prior to weld fracture. Two light punch marks were made at the toe of the weld on the base plate for each LVDT. These punch marks ensured that the two hardened steel anchors of the LVDT brackets sat securely throughout the test. The rear of each bracket had two integrated rollers to stabilize the assembly, while at the same time eliminating longitudinal restraint. The LVDTs were kept in place right up to fracture of one of the test welds in order to acquire the full load versus deformation curve. Failure of the second test weld was not attempted due to the severe connection asymmetry introduced by the failure of the first. The data acquisition system provided a plot of the load versus deformation curve for each of the four LVDT locations in real time to facilitate control of the test. The tests were conducted quasi-statically, with static readings taken at multiple points during the test.

Three lapped splice specimens were tested at –50 °C (–58 °F) following the same procedure described above. A customized chamber was fabricated from rigid insulation to fit the specimen, and dry ice was placed inside to lower the temperature. The surface temperature of the test welds was monitored by thermocouples throughout the test, and the temperature was controlled within ± 3 °C (± 5.4 °F).

After fracture of the weld, the specimen was carefully removed from the testing machine to avoid any damage to the fracture surface. The area of fracture surface, which includes the root penetration, was measured, and measurements of the angle of the fracture surface were taken at eight locations along the weld length using a vernier bevel protractor.

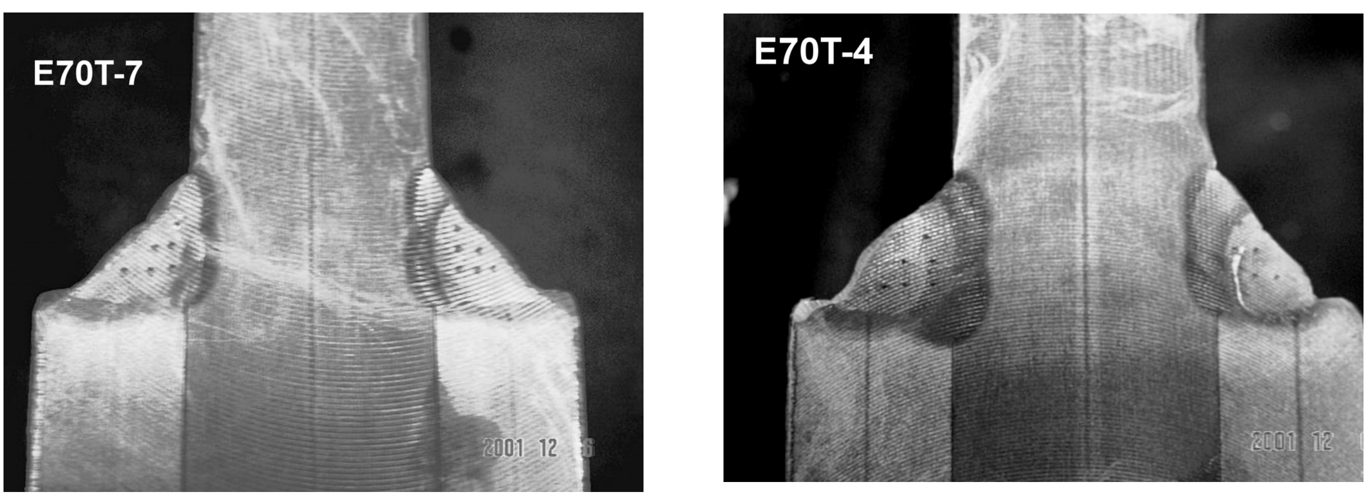
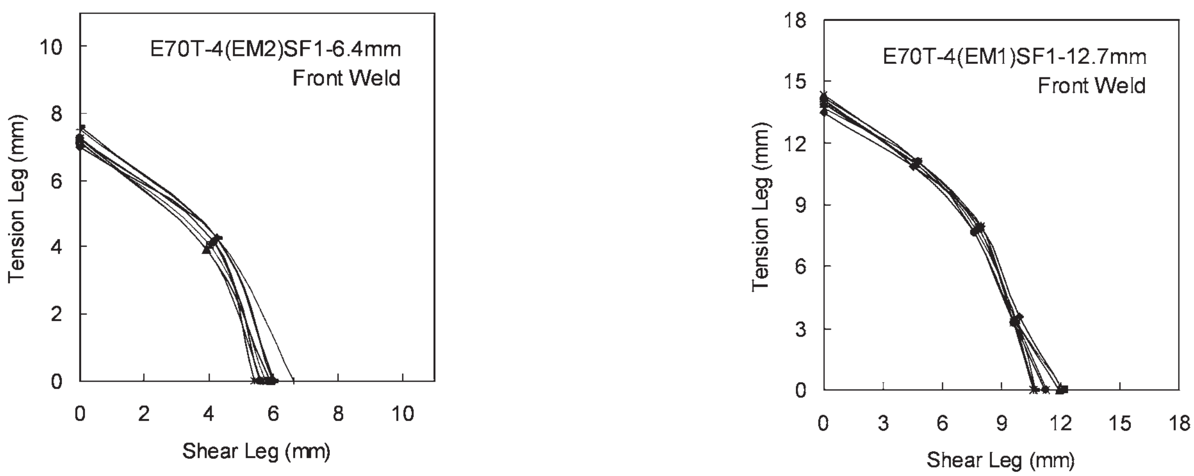
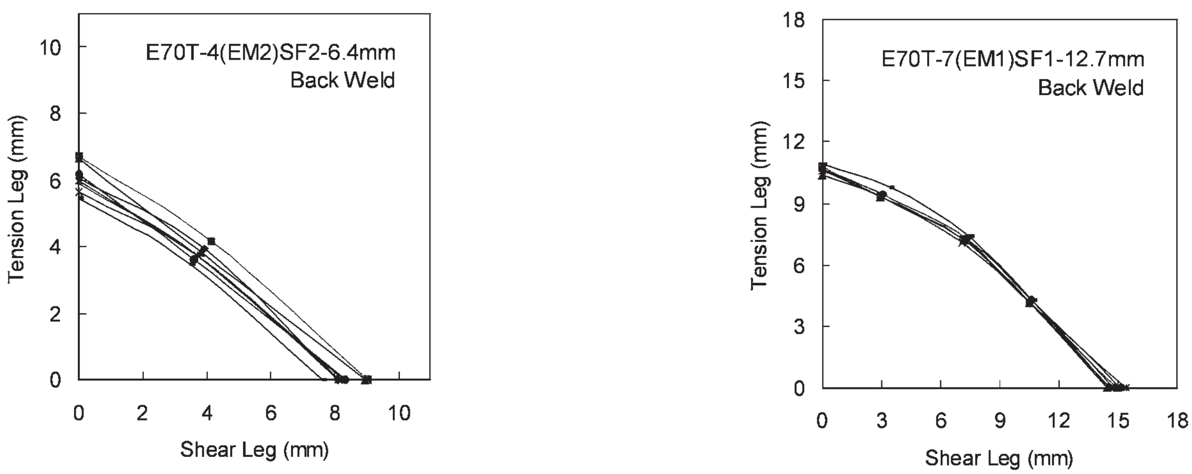


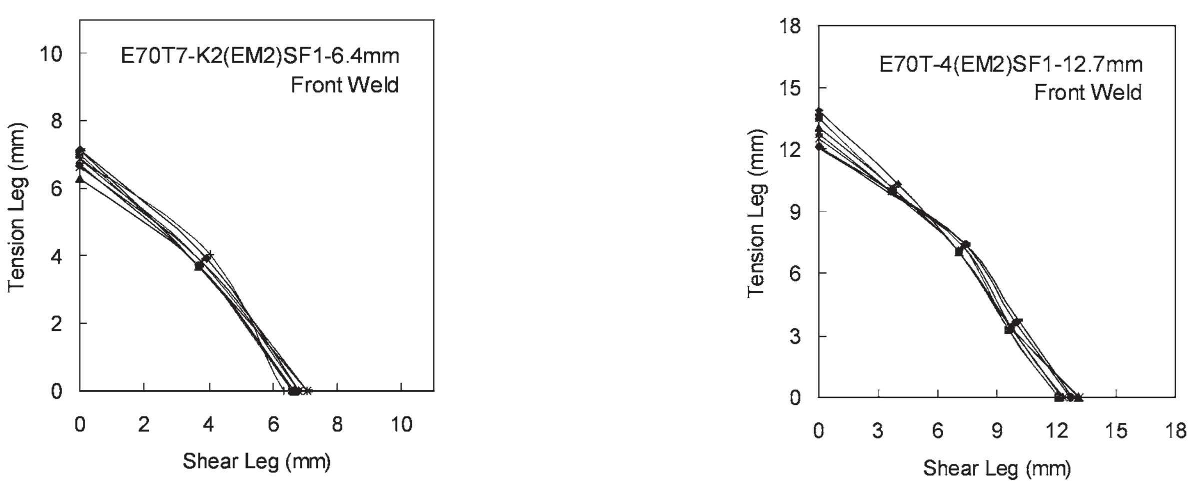
Fig. 2. Root Penetration and Extent of Fusion (12.7 mm welds shown).



(a) Tension leg larger than the shear leg.



(b) Shear leg larger than the tension leg.



(c) Tension and shear legs about the same size.

Fig. 3. Typical Fillet Weld Profiles.

Table 2. Mean Weld Metal Tension Coupon Test Results

Assembly Designation	Filler Metal Classification	Electrode Manufacturer	Steel Fabricator	Mean Static Yield Strength (MPa)	Mean Static Tensile Strength (MPa)	Mean Modulus of Elasticity (MPa)	Mean Elongation (%)
A1	E7014	EM2	SF1	452	520	210 700	21.7
A2 [a]	E70T-4	EM1	SF1	354	535 [b]	185 500	23.2 [b]
A3	E70T-4	EM1	SF2	472	631	198 600	22.3
A4	E70T-4	EM2	SF1	407	562	203 400	27.8
A5	E70T-7	EM1	SF1	468	605	200 800	23.1
A6	E70T-7	EM2	SF1	445	584 [b]	205 200	24.7 [b]
A7	E70T-7	EM2	SF2	483	652 [b]	229 400	22.9 [b]
A8	E70T7-K2	EM2	SF1	527	592	207 100	24.6
A9	E71T8-K6	EM1	SF1	414	490	199 900	27.6
A10	E71T8-K6	EM1	SF2	402	493	207 400	28.4

[a] Five tension coupons from this assembly were tested

[b] Excludes one coupon that fractured prior to reaching the ultimate stress

TEST RESULTS

Ancillary Tests

A total of 23 all-weld-metal tension coupon tests were conducted and a summary of the test results is presented in Table 2. The results presented in the table are the mean values of each set of tests. In three tests, the coupon failed just prior to the ultimate load, as denoted in the table. The static tensile strength values and elongation for these cases exclude the coupons that failed prematurely. The static yield strengths are determined at 0.2 percent strain offset and the elongations are measured on a 50 mm gauge length. In all cases, the mean tensile strength exceeds the minimum requirement in the specifications of 480 MPa (70 ksi), but the amount by which the minimum strength is exceeded varies widely. Only assembly A2 showed a mean yield strength less than the minimum of 400 MPa (58 ksi), as required by the specifications. In addition to the three coupons that fractured prematurely, two coupons (both E70T-4) exhibited ductility somewhat lower than the specified minimum.

An examination of the tension coupon test results indicates that the filler metal manufacturer did not have a significant effect on the strength and ductility, measured as the elongation at rupture, of the filler metal. This can be seen from the results of the two electrode classifications for which a direct comparison can be made: E70-T4 and E70T-7. Despite significant variability, the mean strengths—both

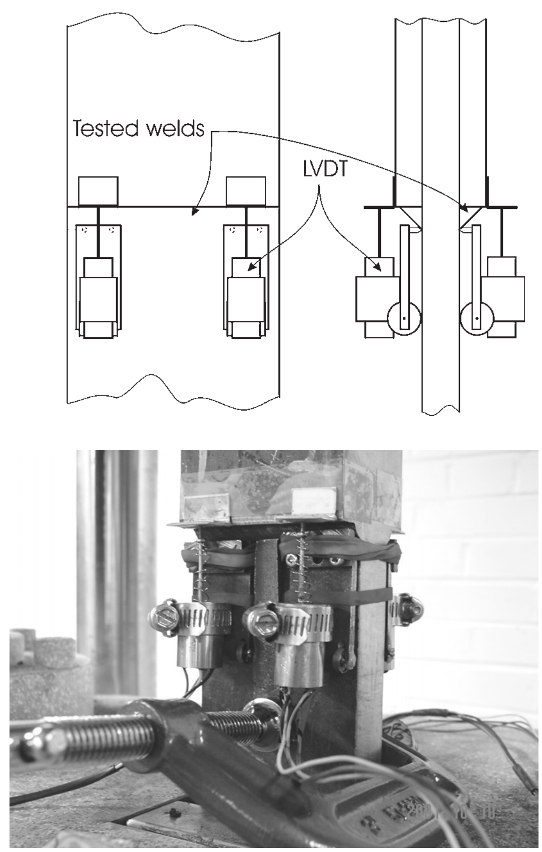


Fig. 4. Test Set-up and Instrumentation.

Table 3. Charpy V-notch Impact Test Results

Assembly Designation	Filler Metal Classification	Electrode Manufacturer	Steel Fabricator	-29°C (-20°F)			21°C (70°F)			100°C (212°F)		
				Energy (J)	Energy (J)	Mean (J)	Energy (J)	Energy (J)	Mean (J)	Energy (J)	Energy (J)	Mean (J)
A1	E7014	EM2	SF1	18	23	20	58	79	68	81	77	79
A2	E70T-4	EM1	SF1	7	7	7	8	8	8	31	27	29
A3	E70T-4	EM1	SF2	9	8	9	15	18	16	57	47	52
A4	E70T-4	EM2	SF1	5	5	5	19	15	17	72	76	74
A5	E70T-7	EM1	SF1	7	5	6	16	15	16	49	56	52
A6	E70T-7	EM2	SF1	11	5	8	24	30	27	62	75	68
A7	E70T-7	EM2	SF2	7	7	7	19	20	20	43	49	46
A8	E70T7-K2	EM2	SF1	34	14	24	75	89	82	165	180	173
A9	E71T8-K6	EM1	SF1	145	140	142	186	186	186	201	214	207
A10	E71T8-K6	EM1	SF2	57	34	45	178	220	199	218	205	212

Note: 1 Joule = 0.73756 ft-lbf

Table 4. Weld Metal Chemical Analysis

Filler Metal Classification	Electrode Manufacturer	Weight (%)										
		C	Mn	Si	P	S	Ni	Cr	Mo	V	Cu	Al
E7014	EM2	0.092	0.260	0.369	0.015	0.0130	0.070	0.055	0.069	0.0200	0.039	<0.010
E70T-4	EM1	0.345	0.295	0.057	0.010	0.0036	0.024	<0.030	<0.050	0.0034	0.016	1.350
E70T-4	EM2	0.272	0.330	0.258	0.009	0.0041	0.012	<0.030	<0.050	0.0026	0.016	1.170
E70T-7	EM1	0.313	0.379	0.065	0.009	0.0032	0.017	0.034	<0.050	0.0046	0.019	1.110
E70T-7	EM2	0.290	0.442	0.093	0.008	0.0037	0.019	<0.030	<0.050	0.0039	0.021	1.140
E71T8-K6	EM1	0.106	0.806	0.088	0.015	0.0043	0.442	0.030	<0.050	0.0052	0.019	0.392
E70T7-K2	EM2	0.087	1.180	0.105	0.012	0.0031	1.190	0.042	0.061	0.0044	0.016	0.738

yield and ultimate—of tension coupons obtained from each of the two electrode manufacturers are within 5 percent for both classifications.

All base plate tension coupon results satisfied the minimum static yield and tensile strength of ASTM A572 Grade 50 (ASTM, 2000) structural steel of 345 MPa (50 ksi) and 450 MPa (65 ksi), respectively. The elongations at rupture also satisfied the minimum requirement of 21 percent.

The Charpy V-notch impact test results are presented in Table 3. The E7014 (SMAW) filler metal had higher impact energies than the FCAW filler metals without a specified toughness at all three temperatures. The E70T7-K2 and E71T8-K6 filler metals (with a specified toughness) generally exhibited much higher impact energies than those without a specified toughness at all three temperatures, although one E70T7-K2 specimen from assembly A8 did not meet the toughness requirement of 27 J at –29 °C.

The chemical composition by weight of each filler metal from both electrode manufacturers is presented in Table 4. In all cases, the element quantities satisfy the requirements of the applicable AWS filler metal specification (AWS 1991, 1995, 1998). As expected, the two filler metals with a

Table 5. Mean Transverse Fillet Weld Test Results

Assembly Designation	Weld Size (mm)	Filler Metal Classification	Mean Ultimate Load (kN)	Test/Predicted[a] (S16)	Test/Predicted[a] (AISC)	Mean Ultimate P/A_{throat} (MPa)	Mean Ultimate $P/A_{fracture}$ (MPa)	Mean Δ/D (Fracture)	Mean Fracture Angle (°)
T1	6.4	E7014	510	1.42	1.59	738	665	0.10	10
T2			473	1.38	1.54	721	706	0.10	11
T3			520	1.27	1.42	666	729	0.09	15
T4		E70T-4	642	1.71	1.91	976	483	0.08	0
T5			636	1.72	1.92	978	457	0.09	0
T6			707	2.00	2.23	1140	846	0.16	86
T7 [a]			635	2.03	2.27	1122	714	0.08	90
T8			698	1.65	1.85	932	613	0.23	9
T9			815	1.94	2.17	1098	830	0.19	31
T10			764	1.75	1.96	1012	747	0.12	21
T11		E70T-7	677	1.53	1.71	930	492	0.11	0
T12			699	1.85	2.06	1021	808	0.13	79
T13			606	1.59	1.77	964	697	0.13	70
T14			752	1.50	1.67	930	521	0.10	6
T15			769	1.63	1.82	1015	528	0.06	0
T16		E70T7-K2	714	1.62	1.81	944	693	0.29	19
T17			738	1.99	2.23	1187	1115	0.09	82
T18		E71T8-K6	707	2.30	2.57	1137	518	0.35	0
T19			769	2.07	2.31	1023	787	0.19	25
T20	12.7	E7014	870	1.15	1.29	602	477	0.16	14
T21		E70T-4	966	1.24	1.39	708	443	0.15	0
T22			936	1.49	1.66	849	488	0.15	0
T23			935	1.21	1.35	682	511	0.18	14
T24			1010	1.41	1.58	798	666	0.20	21
T25		E70T-7	1020	1.29	1.44	783	479	0.13	6
T26			1063	1.35	1.51	822	666	0.20	23
T27			910	1.14	1.28	710	626	0.10	17
T28			993	1.27	1.42	788	494	0.12	6
T29		E70T7-K2	1069	Plate failed prior to weld fracture					
T30			1064	1.45	1.62	886	703	0.22	20
T31		E71T8-K6	1018	1.71	1.91	846	469	0.26	0
T32			1038	1.62	1.81	799	634	0.26	19
C1	6.4	E70T-4	643	1.67	1.86	560	574	0.03	5
C2		E70T7-K2	641	1.56	1.74	446	720	0.03	5

[a] Predicted capacity calculated using the measured weld metal strength from all-weld-metal coupons (method 2)
[b] Specimens tested at –50 °C (–58 °F)

toughness requirement have a significantly higher nickel content and lower aluminum content than those without.

Transverse Fillet Weld Tests

A summary of the transverse fillet weld test results is presented in Table 5, where the mean values for each triplicate test are shown. A more detailed presentation of the test results can be found elsewhere (Ng and others, 2002; Grondin and others, 2002). The lapped splice and cruciform assemblies are designated in the table with a leading "T" and a "C," respectively. The stress, strain, and fracture angle reported in the table are applicable to the weld that fractured first.

The ultimate strength of the weld has been normalized in two ways. In the first normalization method, the ultimate load in the weld (each test weld is assumed to carry one-half of the total force) is divided by the theoretical effective throat area for the mean measured weld leg sizes and the measured length. This area neglects both the penetration and the face reinforcement of the weld, as is done in design. In the second normalization method, the ultimate load is divided by the measured area of the fracture surface. This area accounts for both penetration and reinforcement. In addition, the fracture surface was not oriented at 45°, as assumed in the calculation of the throat area. These factors generally make the area measured on the fracture surface significantly larger than that of the theoretical throat. These capacities are denoted in Table 5 as P/A_{throat} and $P/A_{fracture}$.

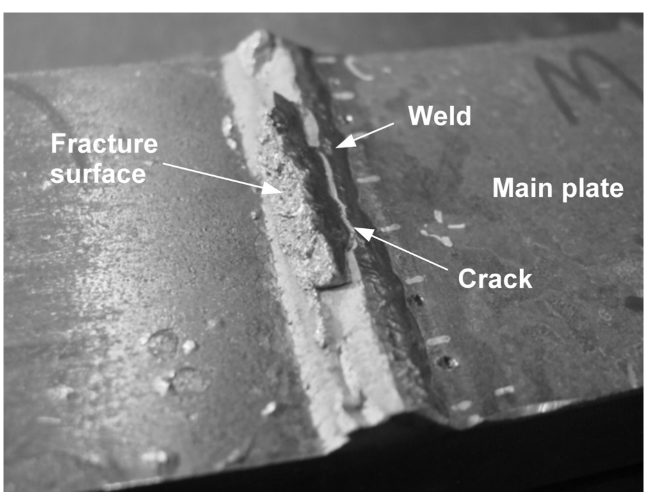
The strain quantity denoted in Table 5 as Δ/D is the mean value of the measured weld deformation at fracture divided by the gauge length (equivalent to the dimension of the leg loaded predominantly in shear) at each LVDT location on the weld that fractured first. In general, weld fractures did not occur on well-defined planes, as shown in Figure 5. This phenomenon was also observed by Miazga and Kennedy (1989). Therefore, mean values of the failure angle (measured from the shear leg) are presented in Table 5.

Most welds failed at an angle less than the angle to the plane that gives the shortest throat distance, although there were several exceptions. The fracture surface was oriented at an angle varying from 0º to 90º. In general, the fillet weld tension leg was somewhat smaller than the shear leg, which can be attributed to the fact that the specimens were welded in the horizontal position. Welds that failed at an angle close to 90° had a tension leg that was significantly smaller than the shear leg.

The specimens tested at –50 °C behaved in a manner similar to the equivalent ones tested at room temperature. However, the strength tended to be amongst the highest in the group, while the ductility tended toward the lower end of the observed range. All three specimens tested at –50 °C failed with a 90° fracture angle.

EXAMINATION OF FRACTURE SURFACES

Examination of the fracture surface of some of the test specimens was performed to determine whether any unusual features may have initiated premature fracture and to determine the mode of failure. All test specimens were subjected to a visual inspection to identify potential areas of interest. Subsequently, a limited number of weld samples, selected during the visual examination, were cut from the specimens and the fracture surface examined in a scanning electron microscope.



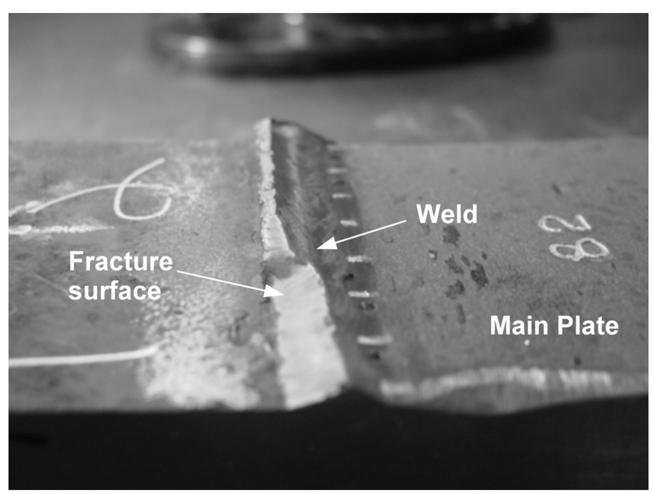


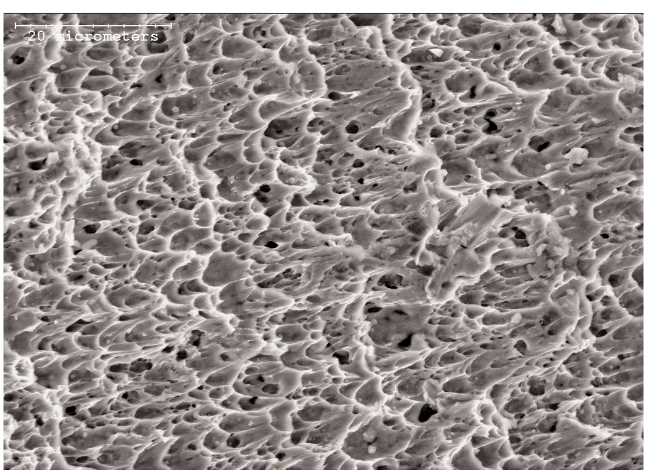
Fig. 5. Non-uniform Failure Angles.

The examinations of the fracture surfaces generally revealed a characteristic ductile fracture surface similar to the one depicted in Figure 6a. The fracture surfaces at 0° and around 20° showed elongated microvoids, indicative of a ductile shear failure. The ductile behavior observed on the fracture surfaces is consistent with the ductility observed in the tests. The test specimens that failed on a 90° fracture surface showed signs of combined ductile and brittle fracture, although they exhibited mostly microvoid coalescence. The photomicrograph in Figure 6b shows a typical fracture surface for this combined failure mode. It can be seen that the microvoids on the left half of the photo are not elongated, which indicates a tension fracture surface. For the welds tested at –50 ºC, close to 50 percent of the fracture surface consisted of cleavage. None of the test specimens examined visually and microscopically showed unusual features.

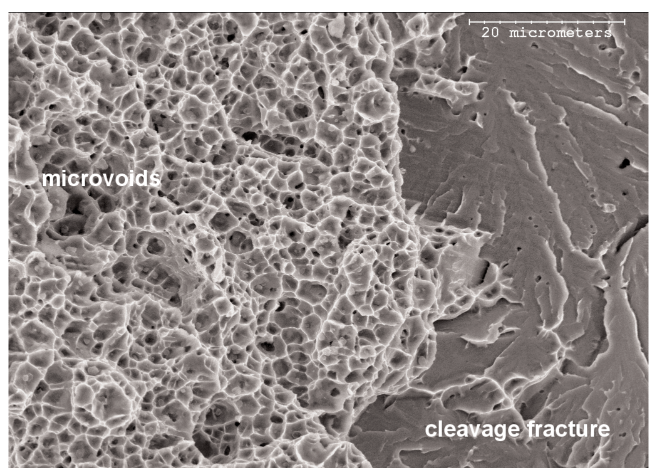
COMPARISON WITH TESTS OF MIAZGA AND KENNEDY (1989)

Some of the test specimens in the current study were fabricated using E7014 electrodes to provide a means of comparing the test results from this investigation with those of Miazga and Kennedy (1989). Table 6 presents a comparison of the welding parameters and properties of the material used to fabricate the test specimens in the Miazga and Kennedy research with those used in the present research program. Miazga and Kennedy tested three specimens with 5 mm (0.20 in.) fillet welds and three specimens with 9 mm (0.35 in.) welds. The present research includes nine test specimens with 6.4 mm welds and three specimens with 12.7 mm welds from E7014 electrodes. An examination of Table 6 indicates that the welding parameters in the Miazga and Kennedy investigation are somewhat different from those used in the current investigation. Although Miazga and Kennedy used different parameters for their two weld sizes, the same parameters were used for the two weld sizes in the present investigation. Since the heat input is inversely proportional to welding speed and directly proportional to current, an estimate of heat input for all four weld sizes listed in Table 6 would indicate that the heat input for the 5 mm weld was significantly lower than that for the 9 mm weld, assuming equal arc voltage. On the other hand, the heat input for the 6.4 mm and 12.7 mm welds was the same, but slightly higher than for the 9 mm weld. Another factor that affects the strength of welds is the number of passes used; the tempering effect of subsequent weld passes decreases weld strength.

The weld material properties obtained from the weld metal tension coupon tests in each investigation are similar. Although the welding parameters used to prepare the weld material specimens are not reported by Miazga and Kennedy, it is expected that the variation in weld metal strength would be smaller than the variation in fillet weld strength because the preparation of the weld metal coupon specimens follows stringent rules with respect to interpass temperature, thus ensuring a more consistent cooling rate among test specimens. The cooling rate, and hence the strength, of the fillet weld specimens would be affected by parameters such as initial temperature, heat input, and plate thickness. These factors were not the same in both investigations. This could result in differences between the two investigations.



(a) Ductile shear fracture (elongated microvoids).



(b) Combination of ductile and brittle tension fracture.

Fig. 6. Photomicrographs of Typical Fracture Surfaces.

Table 6. Comparison of Welding Parameters and Material Properties

Parameter	Miazga & Kennedy		Present Research	
	5 mm weld	9 mm weld	6.4 mm weld	12.7 mm weld
No. of Specimens	3	3	9	3
Welding Parameters				
Speed (mm/min)	250	205	254	254
Current (A)	125	125	170	170
Weld Material Properties				
Modulus of elasticity (MPa)	207 600	207 600	210 700	210 700
Yield strength (MPa)	465	465	452	452
Tensile strength (MPa)	538	538	520	520
Rupture strain (%)	25	25	22	22
Splice Plates Material Properties			[a]	
Yield strength (MPa)	364	346	418	347
Tensile strength (MPa)	522	513	551	466
Rupture strain (%)	23	27	31	38
Main Plates Material Properties				
Yield strength (MPa)	346	324	392	386
Tensile strength (MPa)	513	493	527	538
Rupture strain (%)	27	32	40	41

[a] The plates listed for the 12.7 mm weld specimens were used in three of the nine cases.

A graphical comparison of the test results from the Miazga and Kennedy test program and the present one is presented in Figure 7. The following observations can be made from the figure:

- The scatter in the test results of Miazga and Kennedy, whose specimens were fabricated in a university laboratory by a welder working primarily in research, is significantly smaller than that in the results from the present research.
- In the current research program, the small weld (6.4 mm) exhibits a higher unit strength than the large weld (12.7 mm). This is attributed to the fact that the 12.7 mm weld was deposited in three passes, while the 6.4 mm weld was deposited in a single pass.
- In the Miazga and Kennedy test program the small weld (5 mm) seems to have a lower strength than the large weld (9 mm). This is not expected since the heat input in the 5 mm weld was lower than in the 9 mm weld and the 9 mm weld was deposited in three passes rather than in one, as in the 5 mm weld.
- The larger weld sizes from the two test programs have a similar mean strength. This can be explained, in part, by noting that the heat input for these two welds was similar, compared to the two smaller welds, and that both welds were deposited in three passes, so the cooling rates would have also been similar.

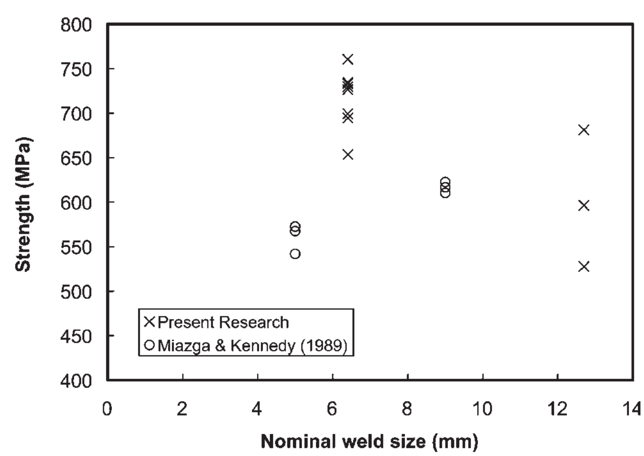


Fig. 7. Comparison of Test Results for E7014 Electrode.

Although differences in strength do exist between the test specimens of the Miazga and Kennedy test program and those of the present test program, the differences can generally be attributed to differences in welding parameters. Insufficient data are available to explain the anomaly observed between the 5 mm and the 9 mm welds from the Miazga and Kennedy test program.

COMPARISON WITH CURRENT DESIGN EQUATIONS

The test results were compared with the design equations in the current North American design standards. Fillet weld capacity is predicted by Equations 2 and 3 [AISC *LRFD Specification* (AISC, 1999) and Canadian standard CSA-S16-01 (CSA, 2001), respectively]:

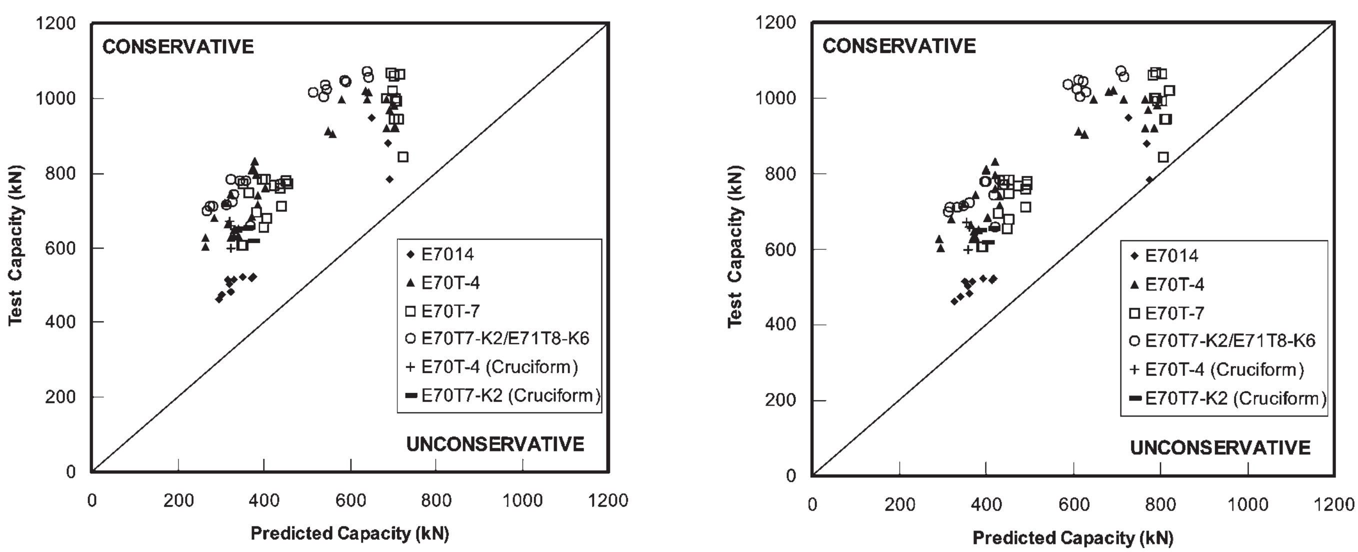
$$V_r = 0.60\phi A_w F_{EXX}(1.0 + 0.50\sin^{1.5}\theta) \tag{2}$$

$$V_r = 0.67\phi A_w X_u(1.00 + 0.50\sin^{1.5}\theta) \tag{3}$$

where ϕ is the resistance factor, A_w is the theoretical throat area, F_{EXX} is the minimum specified tensile strength of the filler metal (same definition as X_u in CSA-S16-01), and θ is the angle of loading with respect to the weld axis. Note that the two design standards differ only in the leading coefficient (0.60 versus 0.67) and the value of the resistance factor.

The results were compared with the standards in two ways. In method 1, fillet weld capacities were predicted using the weld metal nominal strength, 480 MPa in all cases, and in method 2, they were determined using the measured strength from the all-weld-metal tension coupon tests corresponding to the same electrode classification and manufacturer. Method 1 shows the safety margin provided by the design equations by excluding the excess weld metal strength, while method 2 eliminates from the test/predicted ratios the effect that can be attributed directly to overstrength of the weld material itself, which can in some cases be relatively large. This distinction permits an assessment of the expected margin of safety should electrodes be used that have a tensile strength that marginally exceeds the nominal value.

Table 5 shows the mean test/predicted ratios for the two design standards using method 2 only. In the table, ϕ is taken as 1.0, and A_w is taken as the theoretical throat area (calculated from the measured leg dimensions, but neglecting the root penetration and weld reinforcement), and θ is 90°. As shown in the table, all mean test/predicted ratios are greater than 1.0. Although the test/predicted ratios from method 1 are not presented in Table 5 [for details refer to Ng and others (2002)], as expected, these values are even greater than those calculated using method 2. The mean test/predicted ratios using method 1 vary from 1.39 to 2.75 (1.25 to 2.46 for CSA-S16-01). By comparison, the mean



(a) *Predicted Capacity Using AISC* LRFD Specification.

(b) *Predicted Capacity Using CSA-S16-01.*

Fig. 8. Test vs. Predicted Capacity.

test/predicted ratios using method 2 vary from 1.28 to 2.57 (1.14 to 2.30 for CSA-S16-01).

Figure 8 shows a comparison of the test and predicted values for the AISC *LRFD Specification* and CSA-S16-01. Of all filler metals considered, the E7014 filler metal tended to provide capacities closest to the predicted values. E70T-4 and E70T-7 filler metals tended to provide similar test/predicted ratios, and generally higher than the E7014 specimens. Filler metals with a specified toughness (E70T7-K2 and E71T8-K6) tended to provide the highest test/predicted ratios. For the AISC *LRFD Specification*, the mean test/predicted ratios for the E7014, E70T-4, E70T-7, E70T7-K2, and E71T8-K6 electrodes individually are 1.46, 1.80, 1.63, 1.97, and 2.15, respectively, and for CSA-S16-01, the mean test/predicted ratios are 1.31, 1.62, 1.44, 1.75, and 1.86, respectively. The ratios of the mean test/predicted ratio for the 6.4 mm fillet welds to that for the 12.7 mm fillet welds are 1.28 and 1.27 for the AISC *LRFD Specification* and CSA-S16-01, respectively. (In determining the mean test/predicted ratios for the individual filler metal classifications and weld sizes, due to the unique nature of the tests on cruciform sections and on lapped splice specimens tested at –50 ºC, the results from these two special cases are not included.) The ratios of the mean test/predicted ratio for the lapped splice specimens to that for the cruciform specimens are 1.01 and 1.08 for the AISC *LRFD Specification* and CSA-S16-01, respectively. (In determining these values, because cruciform specimens were only produced with 6.4 mm welds and with E70T-4 and E70T7-K2 filler metals, only the results of the 6.4 mm lapped splice specimens with the same filler metals were included.) The ratio of the mean test/predicted ratio for the specimens tested at –50 °C to that for the specimens tested at room temperature is 1.16 for both design standards. (In determining these values, because specimens tested at –50 °C were only produced with 6.4 mm welds and with E70T-4 filler metal, only results of 6.4 mm lapped splice specimens with the same filler metal were included.)

SUMMARY AND CONCLUSIONS

Current North American steel design standards acknowledge that the strength of transverse fillet welds is about 50 percent higher than the strength of equal size longitudinal welds. This increase in strength is supported by the test data from various research programs. However, a review of the literature indicates that although research on the behavior of fillet welds has focused mainly on filler metals without a specified toughness, the level of toughness inherent in the filler metals tested was not systematically evaluated. Because SMAW electrodes were used in most of the research programs, the level of toughness in previous tests may not be representative of FCAW electrodes without a specified toughness. The uncertainty about the effect of weld toughness on the strength and ductility of fillet welds has provided the motivation for the comprehensive testing program presented in this paper.

In total, 102 transverse fillet weld specimens were tested. The test matrix includes 96 double-lapped splice and six cruciform specimens. Test specimens were prepared using five classifications of filler metal: E7014, E70T-4, E70T-7, E70T7-K2 and E71T8-K6. The E7014 filler metal was deposited using the SMAW process and the remainder used the FCAW process. Of the classifications of filler metal tested, only the E70T7-K2 and E71T8-K6 electrodes have a specified toughness requirement. Two weld sizes were included in the lapped splice specimens, namely 6.4 mm and 12.7 mm. Only 6.4 mm fillet welds were included in the cruciform specimens since these tests were considered to be pilot tests to guide future work on the effect of the root notch orientation on fillet weld behavior. Three E70T-4 specimens with 6.4 mm fillet welds were tested at –50 ºC to provide an indication of the effect of low temperature on strength and ductility. In order to provide a broad sample of specimens, electrodes from two manufacturers were used and specimens were produced by two commercial fabricators. All the steel plates used in the test program were designed to yield prior to fracture of the weld, except in the cases of the 6.4 mm E7014 specimens where the base plates were designed to remain elastic in order to conduct a direct comparison with the results obtained by Miazga and Kennedy (1989).

Fracture surfaces of test specimens that fractured at or close to 0° exhibited ductile shear fracture. Fracture surfaces of test specimens that fractured at or close to 90° exhibited tension fracture, with some areas of cleavage. Test specimens that fractured close to 20° showed typically ductile shear fracture surfaces. Cleavage fracture, characteristic of brittle fracture, was more predominant in the test specimens tested at low temperature.

A comparison of the test results obtained in this program with those of Miazga and Kennedy (1989) indicated that, although there are some differences between the two test programs, these can generally be attributed to differences in the welding parameters (welding speed and current).

A comparison of the test results from this research program with the current design equations of Appendix J of the AISC *LRFD Specification* and CSA-S16-01 indicates that the equations are conservative for filler metals with and without specified toughness, and for SMAW and FCAW processes. A detailed analysis of the test results and an evaluation, by means of a reliability analysis, of the level of safety provided by the design equations are presented in the companion paper (Ng and others, 2004).

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