Weldability of High Strength Line Pipe Steels

90 to 100° C preheat eliminates cracking at moderate levels of applied stress when Grade 483 line pipe is misaligned during welding

BY T. H. NORTH, A. B. ROTHWELL, A. G. GLOVER AND R. J. PICK

ABSTRACT. Full-scale weldability tests showed that Grade 483 line pipe material was resistant to cracking up to very high levels of general stress, in the absence of misalignment. When pipe misalignment was introduced a preheat of 90 to 100°C (194 to 212°F) was necessary to eliminate cracking at moderate levels of applied stress.

Full-scale weldability and laboratory weldability test results correlated well when using slot testing and WIC restraint cracking tests. The implant test can give good indications of the susceptibility of a material to HAZ cracking, but its relevance must be questioned in situations where cracking occurs mainly in the weld metal.

Introduction

The field welding of high strength line pipe steels has been studied for many years, and numerous recommendations have been advanced concerning the determination of weldability and the specification of materials to ensure adequate field performance. Much of the recent steel development activity in the line pipe area has been specifically directed towards the improvement of hydrogen-assisted cold cracking resistance, a property upon which heavy demands are made by the still-prevalent stovepipe welding technique.

After nearly two decades of development in this area, it may appear unlikely that anything new can be contributed to the subject. Those close to the pipeline industry, however, will be aware that the problem of cold cracking has far from disappeared; a number of recent, major projects have experienced serious difficulties of this kind. It is legitimate to ask why such a situation still exists, and whether, despite the vastly increased fund of knowledge related to hydrogen-assisted cold cracking, we are still approaching practical problems in the wrong way.

One of the difficulties which arises in addressing the specification of suitably crack-resistant materials is the plethora of formulae which are proposed for the assessment of a material's cracking tendency. While many authorities agree that the IIW carbon equivalent formula is inadequate for modern, low-carbon steels (Ref. 1, 2), most codes and standards and—indeed—most company specifications still depend primarily on this expression. The additional consideration of different ranges of carbon content in some of the more progressive standards and specifications represents a major improvement.

Again, a vast array of laboratory tests have been advanced as providing an assessment of the "weldability" of a material. While some such tests do combine the virtues of simplicity, economy and reproducibility, they are of very little use in the present context if it is not possible to relate the experimental variables and outcome to the way in which cracking arises (and may be prevented) in the real world. Weldability tests that are able to simulate, in full-scale, the exact sequence of welding and manipulation which a pipe may experience in the field are the only ones in which this relationship is direct. Such tests are expensive and cumbersome, however, and only one pipeline company is known to use them on a production basis (Ref. 3, 4).

Lastly, it is important to realize that the one "full-scale" test which is habitually carried out prior to production welding—the procedure qualification test—provides no protection against any but the grossest of inadequacies relative to cold cracking behavior. It is quite unreasonable to assume that the welding of two 500 mm (19.7 in.) long pups, under carefully-controlled shop (or at worst, yard) conditions will give any indication of the likely behavior of 24 m (78.7 ft) long double joints weighing, perhaps, over 10 tons, when welded and manipulated on a roller-coaster right-of-way at subzero temperatures.

It was with some of these perplexities in mind that the present program of work was begun. Initially, a theoretical stress analysis was undertaken in order to determine the sources and relative importance of stresses which could lead to cracking. In particular, a finite element analysis was used to relate local stresses in different regions of the root pass to general bending stress arising from lifting and to assess the effects of weld geometry.

The second phase of the program involved cold cracking tests on full-size samples of line pipe, using a purpose-built, four-point bending frame. Finally, a number of laboratory-scale tests were examined to determine whether any log-

Paper presented on the 63rd Annual AWS Convention in Kansas City, Missouri, during April 26-30, 1982.

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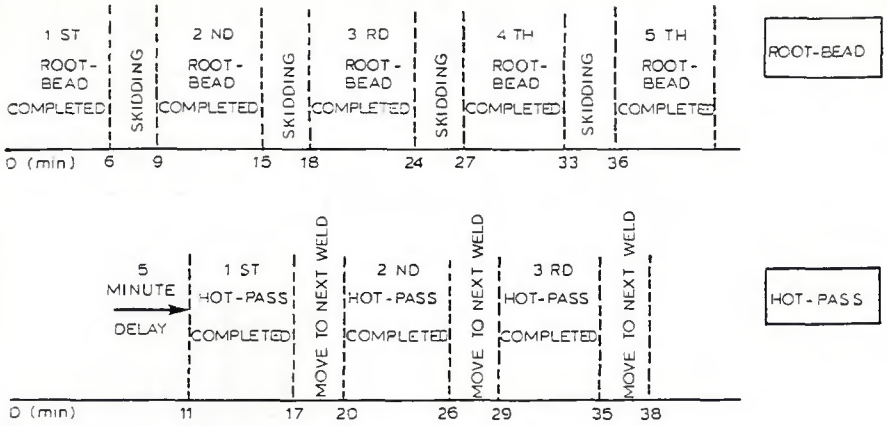


Fig. 1—Schematic representation of root and hot pass welding operations

ical framework existed within which they could provide useful indications of field behavior.

The steels examined, in the experimental part of the work, were typical of those currently being supplied by Canadian pipe makers for use in the construction of demanding pipeline projects such as the Alaska Highway Gas Pipeline System.

Background

Before turning to the analytical and experimental work, it is in order to review the basic factors which affect hydrogen-assisted cold cracking and the way in which they relate to pipeline field welding practices. There is a general understanding of the contributions which the material susceptibility, hydrogen and applied tensile stress make to the probability of cracking (Ref. 5), although the incorporation of this knowledge into quantitative formulations (analogous to those of fracture mechanics) has not yet been accomplished. It is also clear that hydrogen cracking will not normally occur at temperatures in excess of about 100°C (212°F).

Role of Hardness

There has been a widespread tendency to consider hardness as a reasonable indicator of susceptibility to cold cracking. In general terms, the prediction of hardness can be a relatively complex exercise, involving the detailed consideration of heat flow, of hardenability (the relationship between microstructure and cooling rate) and of the hardness of the different microstructures which can develop (Ref. 1, 6, 7).

For the root-pass welding of relatively thick-walled line pipe by the stovepipe technique, the problem is somewhat simplified. This is because the relationship between heat input and wall thickness is such that very high cooling rates through the transformation range can be guaranteed. Consequently, weldability considerations are thus most reasonably based on the maximum hardness which can be developed in a given material.

It must also be realized that cracking can take place in either the HAZ or the weld metal. As a result, pipe materials, which are themselves extremely resistant to cracking, are not always sufficient to ensure freedom from field welding problems. For this reason, laboratory tests which do not force cracking to occur in one region rather than the other are those which should relate most closely to field experience.

Hydrogen Diffusion

Large quantities of hydrogen (≥40 mL/100 g deposited metal) are introduced into the weld region by the cellulosic-coated electrodes used for stovepipe welding. The extent to which this hydrogen can diffuse away from the weld is largely determined by the cooling rate through the low temperature range; this, in turn, is mainly determined by preheat temperature. Düren et al. have shown that an increase in preheat from 20 to 100°C (68 to 212°F) leads to a decrease in diffusible hydrogen content from over 35 to ≈10 mL/100 g (Ref. 8). The addition of a second pass, normally with a significantly higher heat input than the first, before the weld region has time to cool into the cracking range also contributes to the diffusion of hydrogen out of the root-pass region; other results are the softening of hardened microstructures and the reduction of any local stresses resulting from pipe manipulation.

Stress Considerations

Stresses acting on the root pass before deposition of the hot pass could, in principle, arise from a number of sources, and the relative importance of these has not been well understood until quite recently. Bending stresses acting on a pipe due to lifting were considered by Mercer and Needham (Ref. 9), by Lumb and Fearnehough (Ref. 10), and, more recently, by Bufalini et al. (Ref. 11). In the first part of the present program, Higdon et al. (Ref. 12, 13, 15), using simple beam theory, derived the general bending stresses resulting from lifting, in relation to the lifting geometry. They then calculated the local stress concentrations in the root area, using a finite element analysis. They also considered other sources of stress, such as thermally-induced residual stresses and the restrained recovery of ovality when the line-up clamp is released.

The main features of this work are discussed elsewhere in this paper. The important conclusion was that lifting stresses are likely to make by far the most important contribution to the maximum tensile stress acting on the weld; the sequence of welding and manipulation which takes place in a typical field-welding operation must be analyzed in this light.

Stovepipe Welding

The operations associated with the deposition of the first two passes in stovepipe welding, and their timing, have been considered in a recent publication (Ref. 14). They can be summarized as follows:

- A pipe joint is supported at its center by a side-boom and brought up to the end of the already-welded string, where

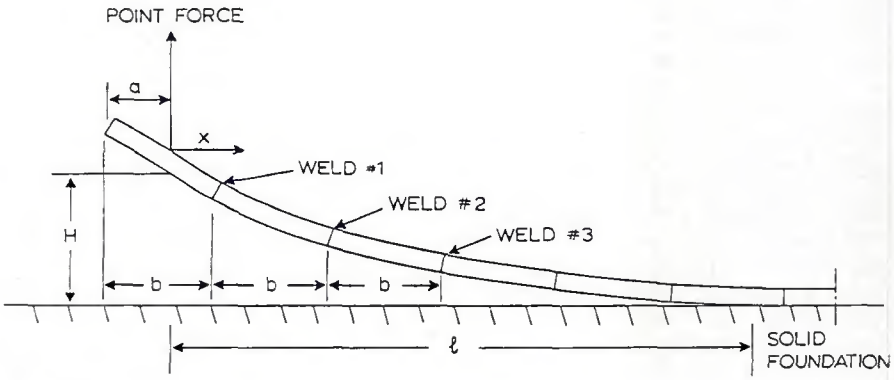


Fig. 2—Lifting of pipe

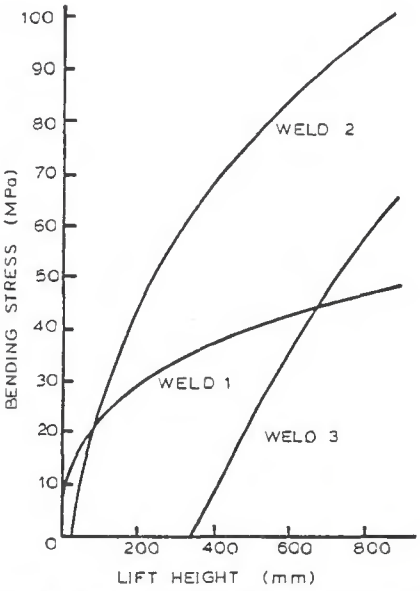


Fig. 3 — Lifting stresses for 18.3 m (60 ft) lengths of 1219 mm (48 in.) diameter, 13.72 mm (0.54 in.) WT pipe

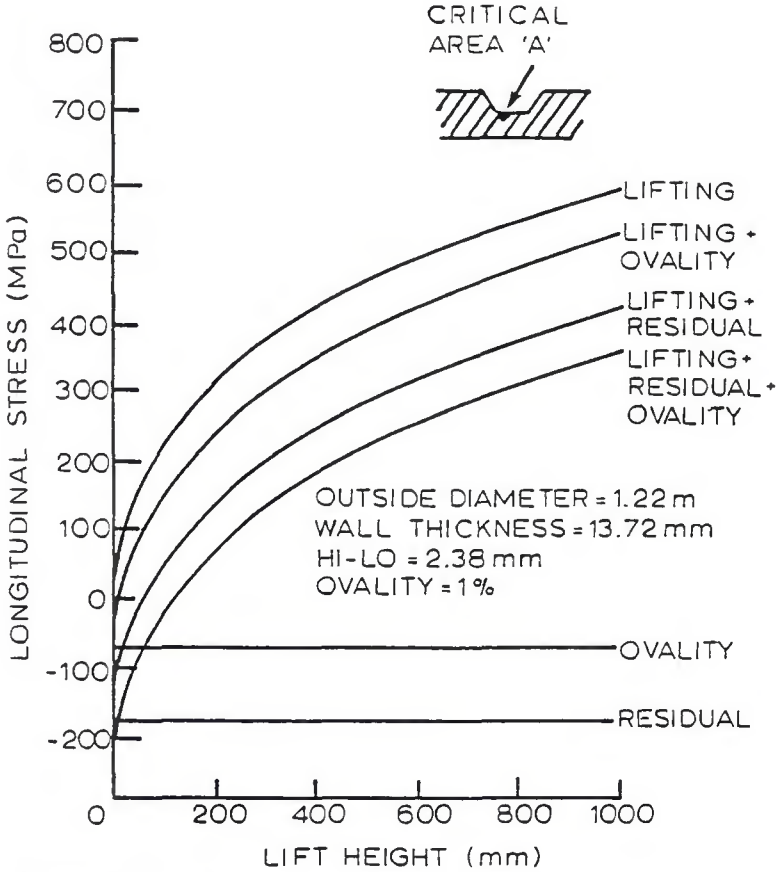


Fig. 4 — Stresses at region A

it is positioned and clamped in place by an internal line-up clamp.

- The root pass is deposited by up to four welders.
- The line-up clamp is released immediately following root pass completion.
- The pipe is lifted and skidded, permitting setup of the next joint.
- The hot-pass is deposited, generally by a crew of three or four welders. It is frequently specified that hot pass deposition must begin within five minutes of completion of the root pass, but this can be difficult to accomplish, with larger pipe sizes and in difficult terrain.

Figure 1 shows this operation schematically; the sequence chosen is typical of a large-diameter project, in which approximately 12 minutes (min) of arc time (3 min for each of four welders) are needed for the root pass, and slightly more for the hot pass. A significant conclusion which arises from this diagram and is in agreement with practical observations, is that two cycles of lifting stress may be applied to the root pass before the critical, 6 o'clock area of the hot pass is completed. If the delay before commencement of the hot pass exceeds 5 min, little of the hot pass will have been deposited before the second lifting cycle has been applied. It will thus be important, in any analysis of stress and hydrogen diffusion, to consider welds at least as far back as two joints from the free end of the pipe string.

Theoretical Stress Analysis

Higdon (Ref. 12) and Weickert (Ref. 13) carried out a detailed analysis of the local stresses acting in the weld root region arising from skidding operations, from release of the line-up clamp, and from residual stresses. Only the principal conclusions are summarized here.

Pipe bending stresses due to lifting were calculated using simple elastic beam theory, as had been done by earlier workers. Figure 2 shows the generalized geometry which was analyzed, while Fig. 3 shows an example of the results — in this case for 18.3 m lengths of 1219 mm (48 in.) OD, 13.72 mm (0.57 in.) wall thickness (WT) pipe. An important feature of this analysis is that the *second* weld (weld No. 2 in Fig. 2) sees rapidly increasing stress levels as lift heights greater than ≈50 mm (≅2 in.); for these pipe lengths, in fact, the stress in the second weld exceeds that in the most-recently deposited weld at quite modest values of lift height. Considerations of cooling conditions, timing of hot pass deposition, as discussed previously, and the practicalities of maintaining adequate interpass temperature suggest that it may often be the last-but-one root pass which is most at risk.

A large number of individual root pass geometries were then analyzed, using a finite element technique, to determine stress concentration factors (SCF) applicable to different areas of the root bead. In addition to the general bending stress, the asymmetric position of the root bead relative to the pipe wall caused maximum tensile stresses at the outside surface (point A in Fig. 4) in the 6 o'clock position. On the contrary, compressive local bending overcame the general tensile stress so that the area in the immediate vicinity of the root was in compression. Local SCF's could reach very high levels (up to ≈20), revealing the inadequacy of simpler approaches using gross stress concentrations of 2 or 3. In practice, of course, yielding would occur, and such high SCF's really imply intense local strain concentrations at relatively low general applied stresses.

Stresses arising from the correction of ovality, release of the line-up clamp and from thermally-induced residual stresses were also considered. By superimposing these on the lifting stress calculated by multiplying the general bending stress by the appropriate SCF, the overall local stress acting at a point in the root pass could be determined. Figure 4 shows an example of the results; in general, lifting stresses were far more important than those arising from the other sources.

The main conclusions of this study concerning the effects of geometry were as follows (Ref. 15):

1. Increasing pipe diameter led to increasing levels of local lifting stress. However, beyond 1067 mm (42 in.) OD, the effect of further increases in diameter was minimal.

2. Increasing the pipe wall thickness, or decreasing weld bead thickness,

Table 1—1219 mm Diameter, 13.72 mm WT Grade 483 Compositions for Full-Scale Weldability Testing, %

Pipe number	Steel grade	C	Mn	P	S	Si	Mo	V	Cb
3A	483	0.070	1.99	0.002	0.006	0.23	0.204	0.051	0.045
45	483	0.055	1.76	0.002	0.003	0.24	0.230	0.045	0.045
57	483	0.090	1.35	0.004	0.002	0.33	0.160	0.089	0.028

sharply increased the local lifting stresses. Since root pass deposition techniques are not usually varied significantly with changing wall thickness, this conclusion has obvious significance concerning the trend towards heavier-wall pipe. A further implication is that root pass welding speeds must not exceed those specified in the qualified procedure; an increase in speed, in the interests of productivity, will reduce the thickness of the root bead, with potentially disastrous results. Adequate and informed field inspection is the only safeguard against such problems.

3. Significant increases in local lifting stresses were brought about by misalignment between the two pipes. This result has extremely important practical consequences, as will be seen in a later section; the real problem posed by out-of-round pipe is not likely to be related to stresses arising from line-up clamp operation, but rather from the inability of the clamp to completely eliminate high-low. On large-diameter, heavy-wall pipe, high-low at the limits of what is permissible under existing codes is a common occurrence. This high-low, by significantly increasing local SCFs, can make a major contribution to cracking. Where possible, high-low should be kept away from the bottom of the pipe.

Experimental Work

Materials

All full-scale testing was carried out on material from three 1219 mm (48 in.) OD, 13.72 mm (0.54 in.) WT, Gr 483, spiral-welded, cold-expanded pipes. The chemical compositions are typical of those currently applied in Canada to the requirements of major orders for large-diameter, high-toughness pipe, and are shown in Table 1. In order to extend the range of chemical compositions studied in the laboratory program, additional materials were tested; these are shown in Table 2.

All full-scale tests were carried out with 4 mm (0.16 in.) E8010G cellulosic manual metal-arc electrodes. This electrode, although it may slightly under-match Grade 483 pipe in all-weld-metal tensile tests (Table 3), is widely used for welding of root passes in these materials.

In the laboratory testing, E6010 and E9010 electrodes were also used in an effort to determine the effect of weld metal strength on cracking characteristics.

Full-Scale Testing

Design of Test Frame. The detailed design of the full-scale test frame has been discussed elsewhere (Ref. 34), and only the most significant features are summarized here.

The general construction is shown in Fig. 5. The bending force is supplied at the outboard load points by two groups of three hydraulic jacks, acting on the pipe through contoured saddles. At the maximum operating pressure of 69 MPa (10 ksi), the force acting at each outboard load point is 1472 kN (330.91 Klb [330,910 lb]); the inboard load is supported by eight 22 mm (0.87 in.) diameter, steel wire cables. In operation, two 6.5 m (21.3 ft) pipe lengths were used; an

Table 2—Steel Compositions Used in Weldability Testing, %

Code[a]	C	Mn	P	S	Si	Cu	Ni	Cr	Mo	V	Cb	P_{CM}
X1	0.069	1.43	0.004	0.005	0.26	0.014	0.012	0.300	0.007	0.053	0.069	0.177
X2	0.077	1.43	0.005	0.005	0.27	0.014	0.011	0.297	0.076	0.053	0.070	0.184
X3	0.071	1.41	0.005	0.005	0.26	0.013	0.011	0.296	0.005	0.053	0.069	0.167
X4	0.078	1.43	0.005	0.005	0.26	0.013	0.010	0.296	0.104	0.053	0.069	0.186
X5	0.068	1.39	0.003	0.005	0.22	0.014	0.010	0.272	0.002	0.043	0.059	0.164
X6	0.068	1.40	0.003	0.007	0.23	0.015	0.014	0.292	0.005	0.046	0.063	0.166
X7	0.07	1.41	0.004	0.006	0.23	0.015	0.011	0.289	0.088	0.047	0.059	0.175
X8	0.063	1.37	0.004	0.01	0.24	0.016	0.012	0.291	0.082	0.046	0.063	0.165
X9	0.064	1.39	0.004	0.009	0.23	0.016	0.013	0.291	0.095	0.046	0.065	0.168
X10	0.065	1.39	0.004	0.007	0.23	0.015	0.011	0.291	0.003	0.048	0.063	0.163
X11	0.145	1.49	0.003	0.014	0.28	0.047	0.023	0.035	0.011	0.051	0.038	0.238
X12	0.12	1.46	0.007	0.012	0.28	0.044	0.028	0.047	0.012	0.04	0.044	0.251
X13	0.115	1.43	0.003	0.015	0.27	0.033	0.02	0.060	0.016	0.036	0.04	0.199
X14	0.06	1.99	0.005	0.003	0.28	0.031	0.016	0.042	0.232	0.002	0.053	0.19
X15	0.07	1.99	0.002	0.006	0.23	0.032	0.012	0.050	0.204	0.051	0.045	0.20
X16	0.066	1.83	0.003	0.002	0.27	0.03	0.013	0.045	0.241	0.051	0.051	0.192
X17	0.055	1.76	0.002	0.003	0.24	0.048	0.017	0.044	0.23	0.045	0.045	0.183
X18	0.061	1.98	0.003	0.002	0.27	0.029	0.015	0.043	0.225	0.005	0.057	0.188
X19	0.061	1.83	0.003	0.002	0.25	0.035	0.013	0.055	0.24	0.005	0.052	0.183
X20	0.057	2.06	0.002	0.005	0.23	0.131	0.023	0.058	0.209	0.03	0.055	0.194
X21	0.076	1.97	0.011	0.004	0.26	0.007	0.034	0.095	0.225	0.053	0.045	0.211
X22	0.09	1.35	0.004	0.002	0.33	0.019	0.017	0.02	0.16	0.089	0.028	0.189
X23	0.074	1.44	0.003	0.006	0.23	0.016	0.013	0.288	0.002	0.044	0.062	0.169
X24	0.078	1.43	0.005	0.005	0.26	0.013	0.01	0.295	0.104	0.053	0.069	0.188
X25	0.088	1.39	0.005	0.002	0.32	0.019	0.009	0.02	0.155	0.088	0.028	0.19
X26	0.106	1.47	0.005	0.007	0.28	0.026	0.012	0.215	0.102	0.05	0.062	0.213
X27	0.11	1.42	–	0.004	–	–	–	0.39	0.11	0.10	0.03	0.218
X28	0.12	1.57	0.009	0.013	0.25	0.06	0.01	–	0.005	0.005	0.035	0.207
X29	0.079	1.59	0.015	0.006	0.34	0.009	0.053	0.008	0.002	–	0.171	0.18
X30	0.09	1.78	0.009	0.005	0.24	0.018	0.013	0.18	0.10	0.05	0.05	0.194
X31	0.084	1.57	0.006	0.005	0.34	0.044	0.021	0.188	0.095	0.047	0.044	0.197
X32	0.061	1.52	0.015	0.005	0.28	–	0.027	–	0.24	0.06	0.044	0.173

[a]Pipe 3A is coded X15; Pipe 45 is coded X17; Pipe 57 is coded X22.

Table 3—All-Weld-Metal Tensile Test Results for E8010G Electrodes[a]

Yield stress, MPa	437.5
Ultimate tensile stress, MPa	520.6
Elongation, %	27.5
Yield stress/ultimate stress	0.84

[a]Phoenix Cel 80 from Thyssen Draht AG.

additional length of pipe was positioned at one end to receive the line-up clamp.

Welding Conditions. All root beads were deposited by four experienced field welders, working simultaneously, and welding downhill, two from the 12 o'clock position and one each from the 3 and 9 o'clock positions. The following welding parameters were generally applied: arc voltage—22 to 24 V; current—160 to 175 A; travel speed—4.23 to 5.1 mm/s (10 to 12 ipm); heat input—0.83 kJ/mm (21 kJ/in.).

Edge preparation was, in general, a standard 75 deg included angle with a 1.6 mm (0.06 in.) root face. Misalignment was reproducibly achieved by placing a 2.36 mm (0.093 in.) aluminum shim between the line-up clamp and the bottom quadrant of one pipe. While this produced a high-low condition above the maximum recommended in the Canadian pipeline code, it is not at all unrealistic relative to what is currently observed on large-diameter, heavy-wall projects.

Testing Technique. Consideration of the stovepipe welding sequence discussed earlier makes it clear that it would be difficult, with a single set of test conditions, to simulate exactly the different thermal and stress cycles which may be experienced by the root pass. It was thus decided to concentrate on the situation experienced by the last-but-one root pass, which is many circumstances will be the most critical. Since temperatures were monitored, it would also be possible to relate the results to the last weld deposited, although a consideration of the cracking conditions reported below would indicate that only for relatively extreme lift heights would this be at risk. The testing technique adopted is described below.

The two pieces of pipe were supported at their inboard ends on mechanical jacks and clamped with a pneumatic, internal line-up clamp. When preheat was required, this was supplied by 6 external gas burners; in general, preheating was continued to 50°C (122°F) beyond the required temperature, and the pipe was then allowed to cool while the welders set up and the pipe was gapped (1.6 mm), using wedges.

When the desired preheat temperature was reached, (measured at the 6 o'clock position), welding was commenced. After completion of welding, the clamp was released and 5 minutes (min) delay was allowed before the start of loading. At this point, the support jacks were released, and hydraulic jack pressure applied; full load was reached in 1 to 2 min. The pipe was held under full load for 15 min or until fracture occurred.

After completion of the hold time, the jack pressure was released. This loading cycle takes account of the fact that loading of the $(n-1)^{th}$ weld root pass may occur at any time within the period between deposition of the nth root bead and completion of the $(n-1)^{th}$ hot pass (see Fig. 1). Under normal circumstances, the delay would not be as long as 15 min, and load application would not be continuous during that period; consequently, the present cycle is likely to be more severe than would usually be experienced in practice.

Applied bending stresses of 43.8, 60.8, and 81 MPa (6.35, 8.82, and 11.7 ksi) were used in the present program; these were produced by jack pressures of 14.7, 20.7, and 27.6 MPa (2.13, 3, and 4 ksi), respectively. The corresponding lift

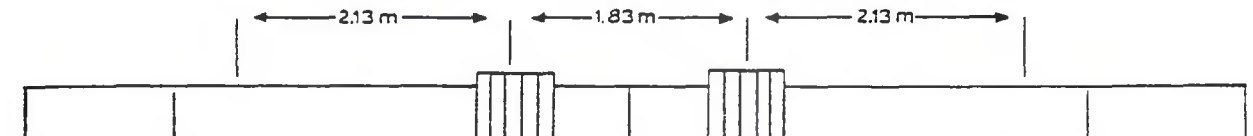


Fig. 5—Design of full-scale test rig

Table 4—Lifting Heights and Pipe Stresses When Laying 12.2 m Lengths of 1219 mm Diameter Pipe

Stress level[a] MPa	Lift height, mm Weld no. 1 H_L	Weld no. 1 H_E	Weld no. 2 H_L	Weld no. 2 H_E	Weld no. 3 H_L	Weld no. 3 H_E
43.1	>1270	>1270	160	216	244	320
60.8	>1270	>1270	333	427	373	478
81	>1270	>1270	673	836	577	719

[a]Stress level indicated below for the tabulated lift height.

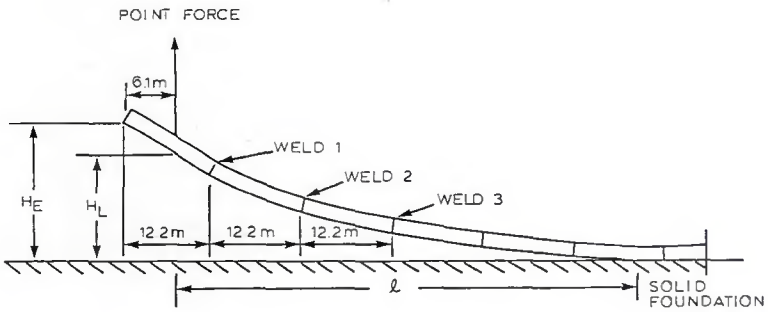
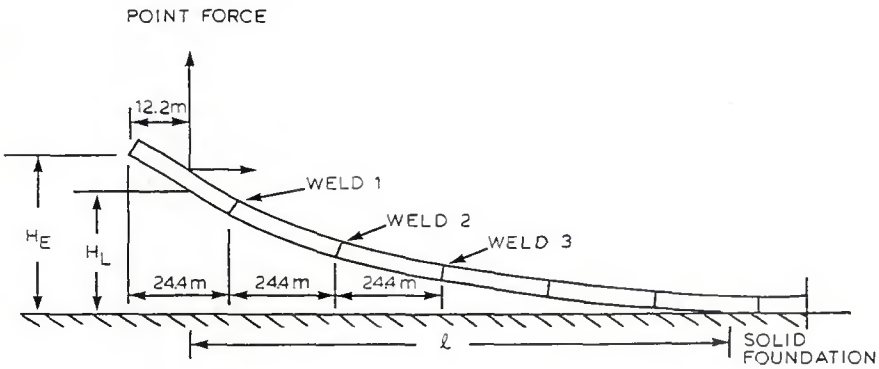


Table 5—Lifting Heights and Pipe Stresses When Laying 24.4 m Lengths of 1219 mm Diameter Pipe

Stress level[a] MPa	Lift height, mm Weld no. 1 H_L	Weld no. 1 H_E	Weld no. 2 H_L	Weld no. 2 H_E
43.1	409	648	333	559
60.8	937	1364	493	765
81	2286	3096	696	1041

[a]Stress level indicated below for the tabulated lift height.



heights for 12.2 m (40 ft) nominal lengths of 1219 mm (48 in.) OD, 13.72 mm (0.54 in.) WT pipe are shown in Table 4, while those for 24.4 m (80 ft) nominal lengths appear in Table 5. Lift heights around 300 mm (11.8 in.) can be considered quite normal, even in relatively favorable terrain.

Welds in which failure did not occur during testing were sectioned and examined metallographically; 18–24 sections were removed from the bottom quadrant of each unfailed test weld.

Laboratory Testing

Four different laboratory test techniques were used; three of these were of the self-restrained type, and allowed cracking to occur in any region of the weld. In addition, some implant testing was carried out, since a number of European and Japanese authors (Ref. 7, 16-19), have suggested that this test may give useful information concerning susceptibility to cracking in field welding.

Slot Weld Testing. Two slot weld testpiece designs were used during the

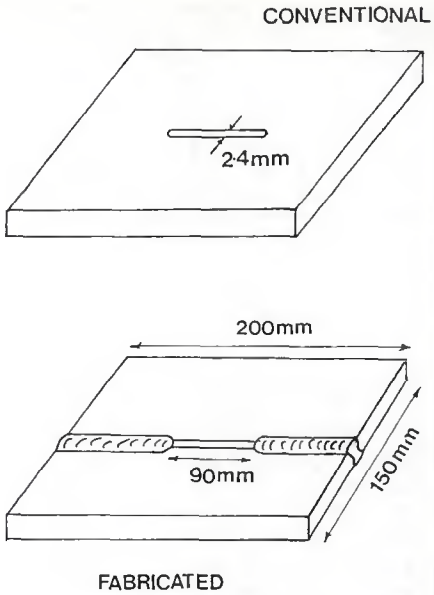


Fig. 6 — Slot test designs

course of this work; they were used as a result of evolution in recommended designs within industry technical bodies. These may conveniently be referred to as "conventional" and "fabricated" slot testpieces.

Figure 6 illustrates the two designs. The fabricated test pieces were made according to the procedure recommended by Vasudevan and Stout (Ref. 20), using low-hydrogen, shielded metal-arc electrodes to deposit the restraining welds from both sides of the testpiece. Testpieces were cut from both pipe and plate. Both manual and automated electrode deposition methods were used; for automatic welding, a specially-designed device was used which maintained constant arc voltage. The testpiece was oscillated under the arc at a little less than 1 Hz during automatic welding, to ensure adequate sidewall fusion.

Welding was carried out using 4 mm (0.16 in.) diameter E6010, E8010-G, and E9010 electrodes. Welding parameters varied according to welding technique, as follows:

1. Manual: 96–108 A, 19–23 V, 2.67–2.88 mm/s (6.3–6.8 ipm).
2. Automatic: 135 A, 24 V, 4.13 mm/s (9.76 ipm).

Heat input was nominally constant at about 0.8 kJ/mm (20.3 kJ/in).

Five minutes after completion of welding, the testpieces were placed in a furnace set at 468°C (874°F); this arrested cracking and tinted any cracked areas. When preheat was required, the testpieces were held in a furnace set approximately 30°C (86°F) above the required temperature; welding was commenced when the required temperature was reached.

Generally, two testpieces were used to establish a percentage cracking value;

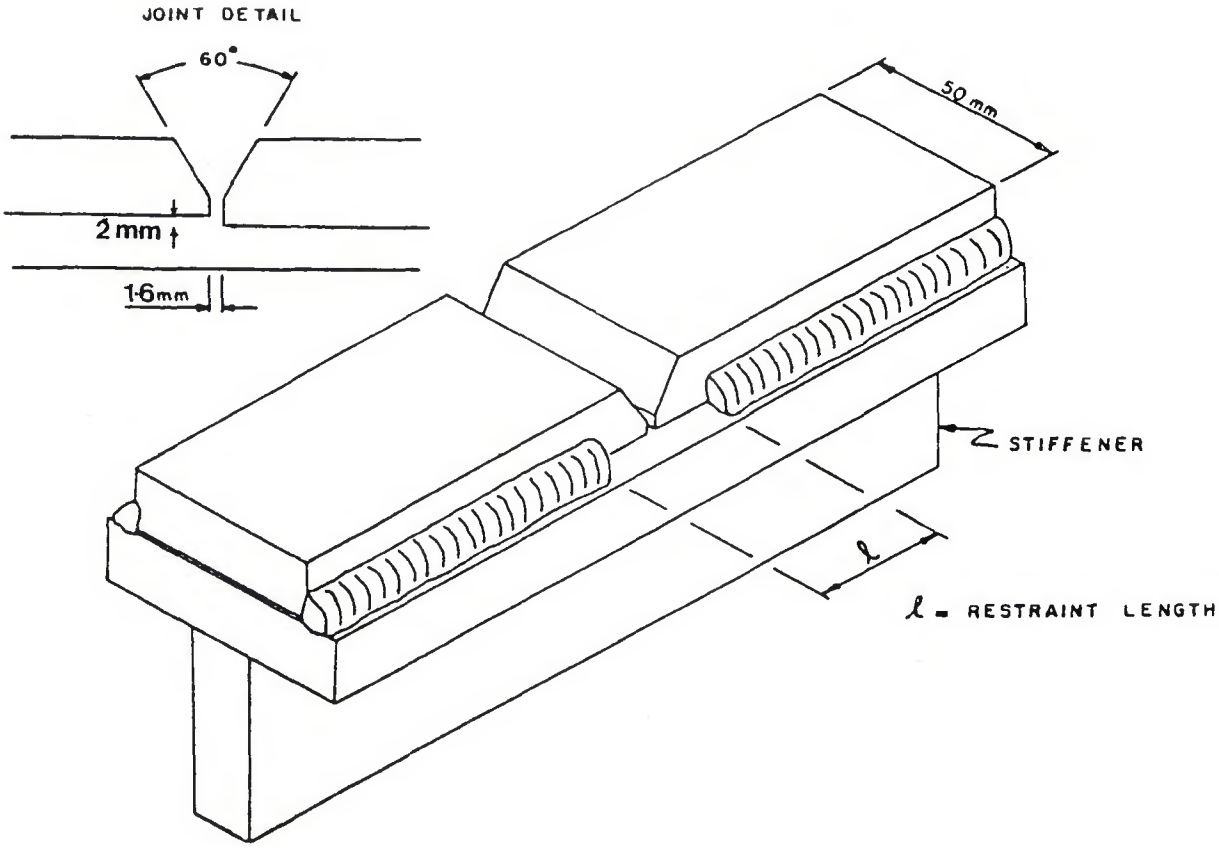


Fig. 7 — WIC restraint weldability test

area percentage cracking was measured using a stereographic microscope after breaking open the testpiece. Preheat temperatures of 66, 93 and 120°C (151, 199 and 248°F) were used throughout this program.

Welding Institute of Canada. Restraint Cracking Test. The configuration of the test was chosen to represent an actual butt weld joint subjected to high reaction stresses. An overall view of the test assembly is shown in Fig. 7. The test consists of two sections of candidate steel (each plate 50 mm wide by 120 mm long i.e., 1.97 × 4.72 in.), with edges already prepared, welded to a base of 75 × 17 mm (2.95 × 0.67 in.) × 250 mm (9.84 in.) long mild steel. A stiffener is added to the bottom of the base to prevent joint rotation. Run-on and run-off tabs are used to eliminate any tendencies for cracking due to weld starting or stopping along the 50 mm (1.97 in.) length of the test weld.

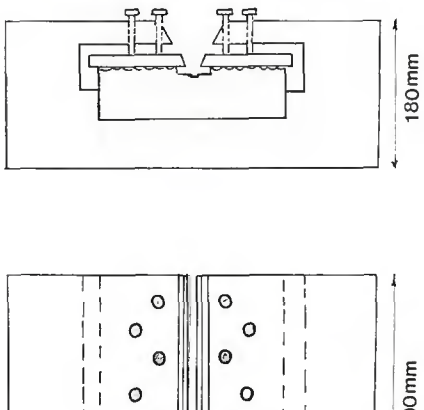


Fig. 8 — Schnadt-Fisco test setup

The welds were deposited using an automatic shielded-metal-arc welding machine. Specimen misalignment was set at 2 mm (0.08 in.) prior to welding. The test assembly was heated in an electrode oven to a temperature slightly higher than the test temperature. The temperature of the joint was recorded using a thermocouple located adjacent to the joint at mid-thickness.

When the steel had cooled to the required temperature, the weld was deposited in the groove. A bead approximately 70 mm (2.76 in.) long was deposited using optimum welding conditions for a E8010-G electrode at a heat input of between 0.70 to 0.80 kJ/mm (17.8 to 20.3 kJ/in.). Once welding had been completed, the specimen was allowed to cool to room temperature.

After 24 hours (h), six specimens from the center section of the weld were metallographically examined. The lengths of the cracks, if cracking occurred, were measured. It was noted whether the cracking was predominantly through the weld metal or through the heat-affected zone.

Restrained Root-Cracking Test (Schnadt-Fisco Test). Restrained root-cracking tests, according to the technique described by Dittrich (Ref. 21), were carried out on the three steels used for the full-scale program. Testpieces measuring 300 × 200 mm (11.8 × 7.87 in.) × 13.72 mm (0.54 in.) thick were loaded into the restraint jig — Fig. 8. The restraint bolts were tightened to a torque of 54 Nm (39.8 ft-lb), and welds were deposited in the downhand position, using an automatic covered electrode-feeder, at a nominal heat input of 0.8 kJ/mm (20.3 kJ/in.).

All welds were made with 4 mm (0.16 in.) diameter E8010-G electrodes. The weld preparation was a single V groove with a 75 deg included angle, with 1.6 mm (0.06 in.) root face and no root gap. Testpieces were kept in the restraint jig for 24 h prior to sectioning and metallography.

Implant Tests. The implant test technique employed in the present work was essentially that described by Sawhill et al (Ref. 22). Implant testpieces 7 mm (0.28 in.) in diameter were machined from both pipe and plate samples. In the case of pipe, they were cut parallel to the pipe

Table 6—Test Results in Full-Scale Weldability Program

Test No.	Pipe No.	Preheat prior to welding, °C	Weld temperature at loading[a], °C	High-low at 6 o'clock position, mm	General pipe stress at weld joint, MPa	Jack pressure, MPa	Equivalent lift height[b], mm	Hold time under load, min	Test results
1	3A	None	–	None	43.08	14.74	160	15	No cracking
2	3A	None	<68	None	60.78	20.69	333	15	No cracking
3	3A	None		None	81.04	27.58	673	2.9	Complete weld failure
4	3A	None	<68	2.38	60.78	20.69	333	4.5	Complete weld failure
5	3A	None	49	2.38	60.78	20.69	333	8.3	Complete weld failure
6	3A	56	75	2.38	60.78	20.69	333	15	Limited cracking at 6 o'clock position
7	3A	75	95.5	2.38	60.78	20.69	333	15	Limited cracking at 6 o'clock position
8	3A	100	117	2.38	60.78	20.69	333	15	No cracking
9	3A	121	108	2.38	60.78	20.69	333	15	No cracking
10	3A	101	94	2.38	81.04	27.58	673	15	No cracking
11	3A	94	118	2.38	81.04	27.58	673	15	No cracking
12	45	54	82	2.38	60.78	20.69	333	0.6	Complete weld failure
13	45	75	102	2.38	60.78	20.69	333	15	No cracking
14	45	75	92	2.38	60.78	20.69	333	15	No cracking
15	57	98	116	2.38	60.78	20.69	333	15	No cracking
16	57	75	100	2.38	60.78	20.69	333	15	No cracking

[a]Weld temperature at 6 o'clock position of pipe circumference.
[b]Lift height during skidding operation producing the general pipe stress at weld no. 2, 12.2 m lengths (refer to Table 5 for 24.4 m lengths of pipe).

axis, while from plate, they were cut at 45 deg to the rolling direction (thus comparable to the axial orientation in spiral-welded pipe). A helical notch 0.50 mm (0.02 in.) deep, with a 1 mm (0.04 in.) pitch and a root radius of 0.05 mm (0.002 in.) was employed.

In tests aimed at relating line pipe composition to critical implant rupture stress, a base metal plate 150 × 100 × 25 mm (5.9 × 3.94 × 0.98 in.) was used. Welds were deposited at a heat input of 1 kJ/mm (25 kJ/in.) using 4 mm (0.16 in.) diameter E8010-G electrodes; this produced a cooling time between 800 and 500°C (1472 and 932°F) of 3.6 s. The load was applied hydraulically when the temperature reached 150°C (302°F). Time to failure was evaluated; if no failure had occurred within 16 h, the test was discontinued and the testpiece was considered unbroken.

Some investigators have suggested that the preheat required to raise the critical rupture strength (σ_{cr}) to the yield point might be an effective indicator of the relative weldabilities of different steel compositions (Ref. 23); moreover, this may also throw light on expected field performance (Ref. 7). For these reasons, further implant tests were carried out on the three steels used in the full-scale program. In this case, the base metal dimensions were 200 × 150 × 13.72 mm (7.87 × 5.9 × 0.54 in.), and the heat input was 0.8 kJ/mm (20.3 kJ/in.); again, 4 mm E8010-G diameter electrodes were used, and loading was commenced at 150°C (302°F). The critical preheat to prevent cracking was taken to be that which inhibited failure for 1000 min. After unloading, implant testpieces were sectioned to confirm freedom from cracking.

Results

Full-Scale Testing

Table 6 summarizes all the results produced during the full-scale field weldability test program. No significantly anomalous results were obtained, and repeat testing under the same conditions generally produced comparable results. Most of the tests were carried out on composition 3A, but some work on the effectiveness of preheat in preventing cracking in compositions 45 and 57 was also done. The main features of the results are summarized below.

Tests Without Misalignment. Even in the absence of preheat, no cracking occurred when general bending stresses up to 60.8 MPa (8.82 ksi) were applied (tests 1 and 2).

Complete failure through the root pass occurred when a stress of 81 MPa (11.7

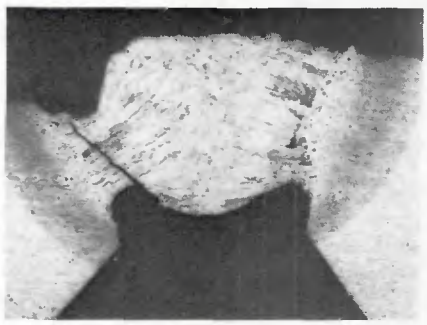


Fig. 9—Crack initiating from toe region in test No. 6

Table 7—Weld Metal and Heat-Affected-Zone Hardness During Full-Scale Testing

Test number	Pipe number	Preheat prior to welding, °C	Weld metal hardness HV 400 g (min)	Weld metal hardness HV 400 g (max)	Heat-affected zone hardness, HV 400 g
4	3A	None	210	274	273
6	3A	56	220	285	295
7	3A	75	216	250	285
9	3A	120	264	309	313
12	45	54	213	270	282
13	45	75	224	264	293
15	57	98	237	250	293

Table 8—Effect of Slot Test Design and Means of Electrode Deposition on the Cracking Percentage (Material Coded X17 Used Throughout)

Test design	Electrode type	Means of electrode deposition	Form of specimen	Cracking, % (Room temp. testing)
Conventional	E8010	Manual	Pipe	41, 33
Conventional	E8010	Automatic	Pipe	48, 47, 30
Fabricated	E8010	Manual	Pipe	25, 40, 12, 21
Fabricated	E8010	Automatic	Pipe	49, 65, 76, 50
Conventional	E8010	Manual	Plate	41, 34

ksi) was applied (test 3). Such a stress would occur at weld No. 2 (Tables 4 and 5) if a 24.4 m (80 ft) length of pipe were lifted 696 mm (27.4 in.) or a 12.2 m (40 ft) length were lifted 673 mm (26½ in.). These are relatively high values for normal operation; the heights corresponding to this stress level for weld No. 1 should be well outside the range which will be encountered in practice except under very adverse conditions (e.g., badly-contoured bends, very severe terrain).

The results tend to demonstrate that, *in the absence of misalignment,* successful welding could normally be accomplished in this material without preheat.

Tests with 2.4 mm High-Low. Incorporation of a 2.4 mm (0.094 in.) local high-low in the lower quadrant led to complete weld failure when a general bending stress of 60.8 MPa (8.82 ksi) was applied. Times to failure in duplicate testing under these conditions were 4.5 and 8.3 min. This stress level corresponds to a lift height of 493 mm (19.4 in.) at weld No. 2 for a 24.4 m (80 ft) joint length, or 333 mm (13.1 in.) for 12.2 m (40 ft) joint length; such values are not unreasonable for normal pipeline operations.

Increasing preheat temperature to 56 and 75°C, i.e., 133 and 167°F (tests 6 and 7) prevented total rupture. However, limited cracking was detected by examining sections from the 6 o'clock area (estimated at ≤10% section length cracking). A preheat of 100°C (212°F) was sufficient to eliminate cracking completely (test 8). When the stress level was increased to 81 MPa (11.7 ksi), a preheat of 94°C (201°F) was sufficient to eliminate cracking. It can be concluded, in general, that a preheat temperature of 90 to 100°C (194 to 212°F) will be sufficient to eliminate cracking in this steel under any practical loading conditions, even in the presence of severe misalignment.

A preheat of 75°C (167°F) was sufficient to prevent cracking in pipes 45 and 57 (tests 12 to 16).

Metallography and Hardness Measurements. Cracking was found to initiate in the outside surface of the weld metal in all cases in which the origin of cracking could be identified (this was done by serial sectioning towards the crack tips); a typical example is shown in Fig. 9. Banks (Ref. 24) observed a similar form of crack initiation during laboratory testing of X60 and X65 pipe, but associated it with high local hardness values and alloy segregation in the vicinity of the root bead toe.

Neither of these effects occurred in our tests. The logical conclusion is that the crack initiation site was determined, as predicted by the theory, by the location of the maximum tensile stress levels. This is not to deny that, in practical cases, lack of fusion or other stress concentrators in the root region may displace the crack initiation site towards the weld root.

Table 7 shows some hardness measurements in both weld metal and heat-affected zone, for a range of preheat temperatures. There appears to be little systematic variation of hardness with preheat temperature; this is in agreement

Table 9—Scatter in Slot Test Data from a Number of Sources (Room Temperature Preheat)

Source	Test Design	kJ/mm	WT, mm	Electrode	Type of steel	Time Delay	Cracking values, %	Range	Std dev.	Mean
Stelco	1 M[a]	0.71/0.87	11.61	E8010	X60	5 min	(10.9) (0)	10.9	7.71	5.45
	1 M	0.71/0.87	11.61	E8010	X60	5 min	(23.9) (30)	6.1	4.3	26.95
	1 M	0.71/0.87	11.61	E8010	X65	5 min	(51.5) (44.5)	7.0	4.95	48.0
	2 M[a]	0.71/0.87	11.61	E8010	X65	5 min	(32.2) (8.6)	23.6	16.7	20.4
	2 M	0.71/0.87	11.61	E8010	X65	5 min	(32.5) (1.9)	30.6	21.6	17.2
	1 M	0.71/0.87	13.72	E8010	X70	5 min	(43.8) (17.8)	26.0	12.02	35.3
	1 M	0.71/0.87	13.72	E8010						
	1 M	0.71/0.87	13.72	E8010	X70	5 min	(40) (33.3)	6.7	4.74	36.6
	1 M	0.71/0.87	13.72	E8010	X70	5 min	(14) (22.6)	11.4	6.08	18.3
	2 A[a]	0.80	13.72	E8010	X70	5 min	(18) (30) (95) (39)	77	34.10	45.5
	2 A	0.80	13.72	E8010	X70	5 min	(55) (20) (45)	35	18.0	40
	2 A	0.80	13.72	E8010	X70	5 min	(72) (60) (53) (52)	20	9.21	59.25
	2 A	0.80	13.72	E8010	X70	5 min	(35) (9) (80)	71	35.9	41.3
German data (Ref. 26)	2 M	0.69/0.79	20.10	E7010	X60	5 min	(100) (100) (100) (100) (98) (100) (98)	10	3.73	98.3
	2 M	0.69/0.79	20.10	E7010	X65	24 h	(20) (10) (60) (35) (60) (10) (54)	50	22.7	35
	2 M	0.69/0.79	16.51	E7010	X70	24 h	(32) (38) (28) (50) (65) (25) (15)	50	16.75	36
US data (Ref. 25)	1 M	1.20	30.5	E7010	X65	24 h	(40) (95) (100)	60	33.3	78.3
	1 M	1.20	30.5	E7010	X65	24 h	(60) (70) (90)	30	15.28	73.3
	1 M	1.20	30.5	E7010	X65	5 min	(20) (75) (90)	70	36.8	61.7
Australian	1 M	0.51/0.59	8.38	E7010	X65	6 min	(30) (40) (25)	15	31.7	7.6
data (Ref. 27, 28)	1 M	0.51/0.59	8.38	E7010	X65	6 min	(29) (22) (48)	26	13.5	33
	1 M	0.51/0.59	8.38	E7010	X65	8 min	(100) (100) (8)	92	53.1	69.3
	1 M	0.51/0.59	8.38	E7010	X65	8 min	(71) (33)	58	41	62

[a] 1 M – manual, conventional slot design; 2 M – manual fabricated slot design; 2 A – automatic, fabricated slot design.

Table 10—Critical Preheat Temperatures Avoiding Cracking during Slot Testing (5 Minute Hold Time in Each Case)

				Electrode Type[c]			Electrode Type		
Code	Wall mm	P_{CM}[d]	Test method	Designation	Cracking range (no preheat), %	Critical preheat temp., °C	Designation	Cracking range (no preheat), %	Critical preheat temp., °C
X1	11.6	0.171	1 M[b]	E6010	6 to 9	66	E8010	<1[a]	66
X2	11.6	0.184	1 M	E6010	<1[a]	66	E8010	12 to 19	66
X3	11.6	0.167	1 M	E6010	<1[a]	66	E8010	<1[a]	66
X4	11.6	0.186	1 M	E6010	3 to 4	66	E8010	12 to 14	66
X5	11.6	0.164	1 M	E6010	0 to 11	66	E8010	<1[a]	93
X6	11.6	0.166	1 M	E6010	8 to 17	66	E8010	<1[a] to 2	66
X7	11.6	0.175	1 M	E6010	7 to 21	66	E8010	<1[a]	66
X8	11.6	0.165	1 M	E6010	<1[a]	66	E8010	1 to 6	66
X9	11.6	0.168	1 M	E6010	24 to 31	66	E8010	5 to 15	66
X10	11.6	0.163	1 M	E6010	2 to 14	66	E8010	5 to 28	66
X11	11.6	0.238	1 M	E6010	31 to 33	120	E8010	15 to 27	120
X12	11.6	0.209	1 M	E6010	5	66	E8010	3 to 9	93
X13	11.6	0.199	1 M	E6010	14 to 28	93	E8010	1 to 3	93
X14	13.7	0.19	1 M	E6010	18 to 44	93	E7010	27 to 53	93
X15	13.7	0.20	1 M	ND	ND	ND	E8010	30 to 50	93
X16	13.7	0.192	2 A[b]	E9010	16 to 58	93	E8010	9 to 80	93
X17	13.7	0.183	2 A	ND	ND	ND	E8010	49 to 76	93
X18	13.7	0.188	2 A	ND	ND	ND	E8010	6 to 22	66
X19	13.7	0.183	2 A	ND	ND	ND	E8010	6 to 22	66
X20	13.7	0.194	2 A	E9010	ND	93	E8010	2 to 6	93
X21	13.7	0.211	1 M	ND	ND	ND	E8010	27 to 40	120
X22	13.7	0.189	1 M	ND	ND	ND	E8010	15 to 23	66

[a] <1% but definitely cracked.
[b] 1 M is conventional slot specimen, manually welded; 2 A is fabricated slot specimen, automatic welded.
[c] ND = not determined
[d] $P_{CM} = \%C + \frac{\%Mn}{20} + \frac{\%Si}{30} + \frac{\%Ni}{60} + \frac{\%Cr + \%Cu}{20} + \frac{\%Mo}{.15} + \frac{\%V}{10} + 5B$

with the general view that the beneficial effect of preheat in low-hardenability steels is primarily a result of the increased opportunity for hydrogen diffusion, rather than of any decrease in susceptibility. In this regard, the weld temperatures at the onset of loading, five min after root pass completion, are of some interest—Table 6.

Results of Laboratory Tests

Slot Weld Testing. Table 8 shows the effect of electrode deposition technique and testpiece form on percentage cracking when welding with E8010 electrodes at room temperature [material X17 (Table 2) used throughout]. Within the limitations of the experimental error, there did not appear to be any very significant effect of testpiece type, source (pipe or plate) or welding technique (manual or automatic) when using the conventional slot test design. In the case of fabricated slot specimens, cracking percentages were higher in automatic welds than in manual welds; deposit shapes were examined and manual welds were deeper and narrower than those made automatically.

One of the factors affecting the use of cracking percentage as a comparative indicator of weldability is the considerable scatter associated with these measurements, which would necessitate a large number of replications in order to obtain a reliable estimate. Table 9 shows slot test data from a number of sources

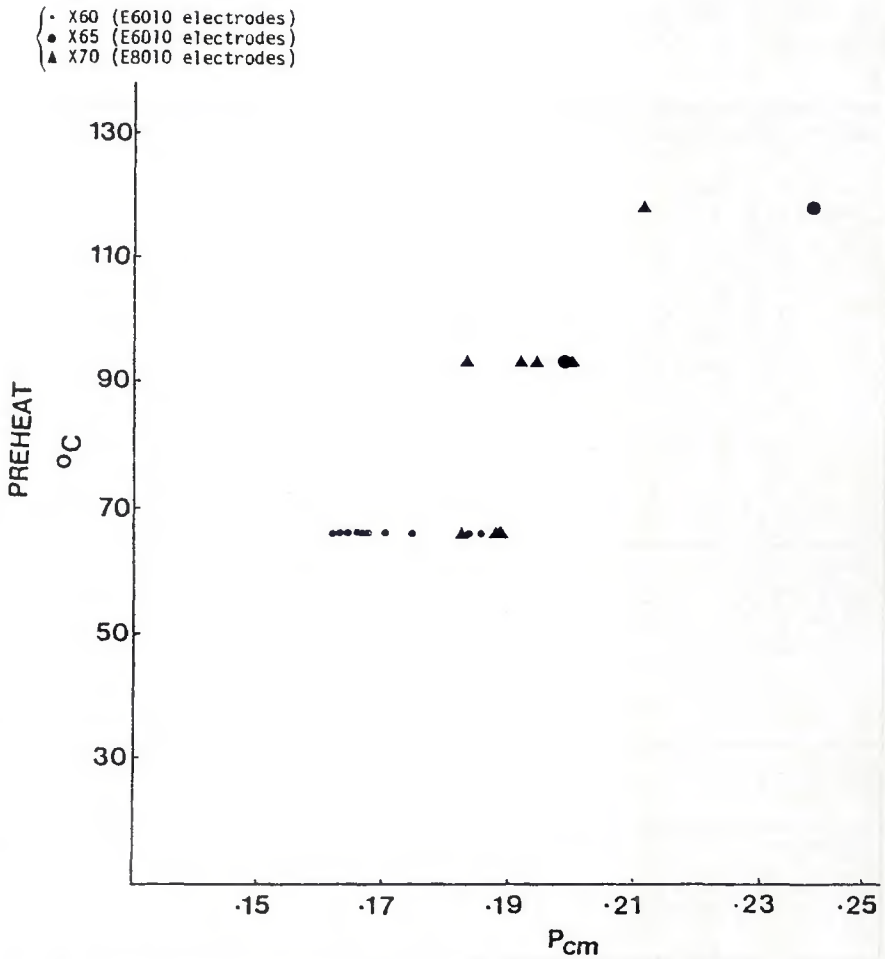


Fig. 10—Critical preheat temperature and P_{CM} values during slot weldability testing

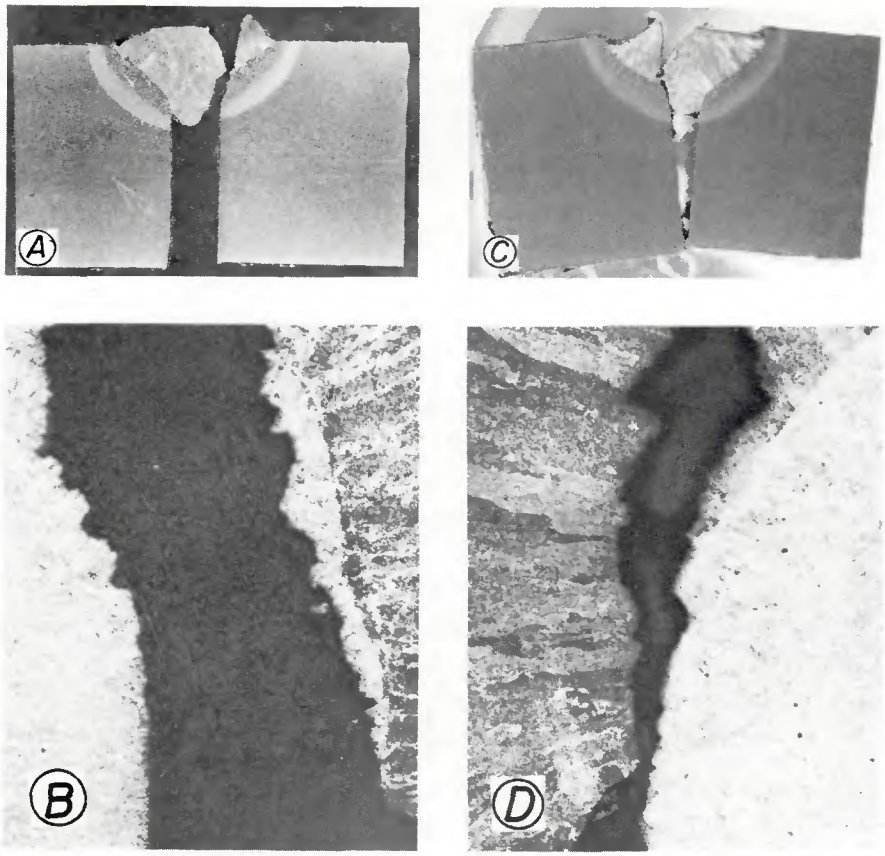


Fig. 11—Failure during slot testing A—macro view (E6010 electrodes); B—closeup showing HAZ failure; C—macro view (E9010 electrodes); D—closeup of fracture region showing failure initiating at the fusion line and propagating into the weld metal

Table 11—WIC Restraint Test Results

Pipe material	Preheat temperature, °C	Test result	Average HAZ hardness values, HV 5[a]
3A	20	Cracked	303
	105	Cracked	285
	120	Uncracked	296
	150	Uncracked	281
45	20	Cracked	289
	100	Uncracked	275

[a]5 kg load during hardness testing.

(Ref. 25-28). The reproducibility is, in general, not very satisfactory except at very high or very low levels of cracking. The discrimination capability of this criterion is thus poor in the intermediate range. In fact, in our own tests (Table 9), there was no clear-cut effect of chemical composition on cracking percentage.

This difficulty is overcome by using the critical preheat to avoid cracking as the weldability criterion. As discussed later, this parameter relates directly to something that can be controlled in the field. As can be seen from Table 10 (and Fig. 10), there was a general increase in critical preheat temperature as P_{CM} increased. Yurioka (Ref. 1) observed a similar trend when testing Japanese steels and suggested the following relation:

$$\text{Critical Preheat Temperature (°C)} = 718\ P_{CM} - 88$$

This relationship was derived when testing a range of steels with varying P_{CM} values (from 0.124 to 0.358) using E7010 electrodes.

In our results the relationship between preheat temperature and P_{CM} was:

$$\text{Critical Preheat Temperature (°C)} = 747\ P_{CM} - 58$$

This equation applies to slot testing using E8010 electrodes and incorporates a delay time of 5 min after welding.

Within the accuracy of the present experiments (≈30°C or 86°F steps in preheat temperature), there was no effect of electrode type on the preheat temperature required. This is somewhat surprising, since the type of cracking changed from predominantly HAZ cracking with E6010 electrodes, to a mixture of weld metal and HAZ cracking with E8010 electrodes, to essentially weld metal cracking with E9010 electrodes—Fig. 11; all cracking initiated at the weld root. Düren (Ref. 26) has noted a similar effect.

WIC Restraint Cracking Tests. These tests were carried out on pipe 3A, with cross checks on pipe 45. Table 11 gives the results produced during testing.

All cracks occurred predominantly in the weld metal. The critical preheating temperature for avoiding cracking in pipe 3A material was 120°C (248°F); in pipe 45 material it was between 20 and 100°C (68°F and 212°F). These results on pipe 3A were approximately 25/30°C (77/129°F) above those required in full-scale weldability testing.

Implant Testing. Figure 12 relates the critical implant rupture stress to the P_{CM} compositional parameter, for welds deposited at 1.0 kJ/mm (25.4 kJ/in.) on 25 mm (≅1 in.) plate, without preheat; the excellent correlation between σ_{cr} and P_{CM} has been observed by other workers (Ref. 16-18). Others (Ref. 29) have found a much better relationship existing between σ_{cr} and HAZ hardness. Figure 13 shows this relationship, for the present work, to be similar to that for P_{CM}.

The tests carried out to determine the critical preheat temperature to raise σ_{cr} to σ_y for the three steels used in the full-scale investigation gave the results shown in Table 12. Critical preheating temperatures avoiding complete specimen rupture in 1000 min were 150, 120 and 150°C (302, 248 and 302°F) for steels 3A, 45 and 57. Although preheating temperatures up to 150°C (302°F) decreased the cooling rate in the 800 to 500°C (1472 to 932°F) range there was no clear-cut effect on HAZ hardness values.

Critical preheating temperatures avoiding failure in a 1000 min holding period during implant testing were much higher than those required during full-scale testing. An obvious problem lies in the comparison of a long term (1000 min) implant test with a short term (15 min) full-scale testing situation. Preheating temperatures to avoid implant specimen failure during a 15 min test period can be estimated by interpolation as noted below, since there is a linear relation between preheat temperature and the logarithm of the time (t) to failure in implant testing (up to 1000 min):

- Steel 3A preheat temperature (°C) = 17.8 $\ln t_{(min)}$ + 32
- Steel 57 preheat temperature (°C) = 16.1 $\ln t_{(min)}$ + 44
- Steel 45 preheat temperature (°C) = 12.8 $\ln t_{(min)}$ + 30

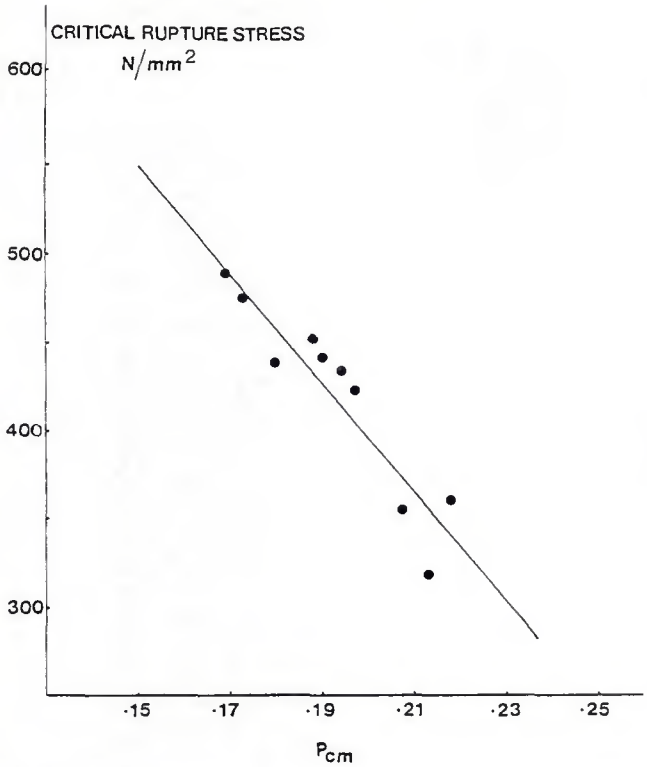


Fig. 12 — Relation between critical rupture strength in implant testing and P_{CM}

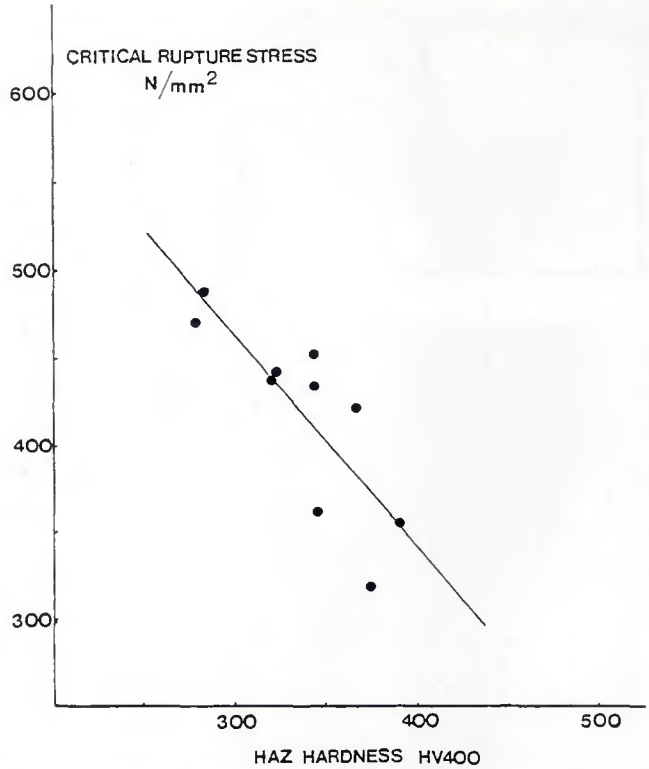


Fig. 13 — Relation between critical rupture strength in implant testing and HAZ hardness

Correlation coefficients are 0.92 (steel 3A), 0.90 (steel 57) and 0.94 (steel 45). From these relations, the estimated preheat temperatures preventing implant specimen failure during a 15 min holding period were 80, 88 and 65°C (176, 190 and 149°F) for steels 3A, 57 and 45.

Restrained Root Cracking (Schnadt-Fisco Test). The critical preheat values determined for the three steels tested in the full-scale program were 35, 22 and 35°C (95, 72 and 95°F) for the steels 3A, 45 and 57, respectively.

Discussion

Full-Scale Testing

As pointed out, the sequence of testing adopted in the present program represents a particularly severe situation. In particular, the sustained loading over a 15 min period is a very exacting test, and should represent a significant factor of conservatism in predictions derived from this type of testing. On the other hand, however, the loading by the hydraulic jacks was a relatively smooth, gradual process, while the manipulation of large masses by relatively insensitive equipment in the field can lead to significant inertial loads.

These effects may be expected to offset each other to some extent, and the practical implications of the present program are thus likely to be reasonably representative of the circumstances arising during field construction. Certainly, empirical experience in Canada has led to the widespread adoption of preheat temperatures around 100°C (212°F) for the manual welding of these materials; when these preheats are conscientiously applied and carefully maintained, the incidence of cracking has been negligible. The cost of this practice can be estimated as lying in the region of $10 to $20 per weld. If this is compared with the cost of even a limited number of cut-outs, not to mention the quality implications of a high incidence of detected cracks, it must be concluded that a generous preheat represents a sound investment.

It may be considered that special attention to hot-pass techniques, to ensure that the 6 o'clock region is completed prior to the application of the second lifting cycle, may be sufficient to avoid cracking difficulties. Such practices will certainly be helpful, but may still be insufficient, in the absence of adequate preheat. In particular, it should be considered that very severe conditions (heavy-wall pipe, high lift) may cause cracking in the last weld deposited, which cannot be protected by hot-pass deposition. In fact, unpublished work by Stelco on the welding of 18.3 mm (0.72 in.) WT, Gr 483 pipe at low heat inputs (<0.44 kJ/mm, i.e., 11.2 kJ/in.) showed that cracking could be produced in the last weld deposited with quite modest lift heights (approximately 280 mm or 11 in.) even at a preheat of 80°C (176°F).

Comparison Between Full-Scale and Laboratory Tests

A direct comparison of laboratory-scale tests with full-scale behavior may be expected to be difficult, since the basic principles involved are somewhat different. In the field welding situation, the external restraint is minimal, since the axial movement of the pipes is not significantly hindered (particularly after line-up clamp release). Only at the closure points will relatively high levels of self-restraint, due to the presence of previously deposited weld metal, be expected to arise.

By contrast, most of the tests proposed for the laboratory assessment of field weldability are of the restraint-cracking type, in which stresses are induced by contraction of the welded region against the resistance of the surrounding parts of the testpiece itself, or of an external restraining jig. The implant test differs in that it does involve a directly-applied external stress; however, because of its design, it concentrates cracking in the HAZ region, while the restraint tests address both weld metal and HAZ.

From previous discussion, it should be clear that critical preheat will be the preferred test parameter to relate to full-scale behavior. The main reason, from a practical point of view, is that preheat is the one parameter which can be readily controlled in the field to eliminate problems. It is much less feasible to change heat input, restrict lift heights, or place very tight limits on the timing of weld passes or lifts. In addition, a number of investigators have shown that, for low susceptibility materials, cracking is extremely sensitive to preheat (Ref. 5).

In the present program, a change in

Table 12—Critical Preheating Temperatures During Implant Testing

Pipe material	Preheat temperature, °C	Time to failure, min	Peak hardness in HAZ,[b] VPN	Cooling time between 800 and 500°C, seconds
3A	20	<1	270	4.3
3A	65	19	344	–
3A	90	47	–	–
3A	115	54.1	–	–
3A	125	34.5	322	5.6
3A	140	Not failed	–	–
3A	140	110	–	–
3A	150	Not failed[a]	322	7.2
3A	150	Not failed[a]	–	7.2
45	20	<1	–	4.3
45	55	12.5	–	–
45	72	18.5	–	–
45	90	38.7	–	–
45	100	Not failed	316	5.3
45	100	78.2	–	5.3
45	120	Not failed[a]	237	–
45	120	Not failed[a]	–	–
57	20	<1	–	4.3
57	72	18.5	–	–
57	90	12.5	–	–
57	100	9.6	274	5.3
57	125	41	328	5.6
57	140	75.1	–	–
57	140	Not failed[a]	–	–
57	150	Not failed[a]	285	7.2
57	150	Not failed[a]	322	7.2

[a]In 1000 minute loading period.
[b]400 g microhardness testing.

preheat of ≈30°C (86°F) was often sufficient to go from complete rupture to complete absence of cracking. It is probably for this reason that measurements of cracking percentage in the intermediate range (10 to 90%) are subject to such severe scatter.

Vasudevan (Ref. 20) has succeeded in establishing a general correlation between cracking percentage in slot testing with that during shop welding of pipe sections (in the absence of externally applied stress from pipe skidding operations). This correlation was far from clear at low cracking percentages, although less than 10% cracking in slot testing corresponded to zero cracking during pipe welding. Very clear relationships emerge, however, when critical preheat is used as the indicator of cracking tendency.

Table 13 compares the critical preheat temperatures determined during full-scale and laboratory weldability testing. The following major points emerge:

1. Slot testing ranked the three steels and produced values of critical preheat temperature correlating well with those determined in full-scale testing.
2. The WIC restraint cracking test gave critical preheat values which were similar to, but somewhat higher than, those obtained from full-scale testing.
3. The Schnadt-Fisco test required minimal preheat to eliminate cracking.

It seems clear that the self-restrained tests can provide a good indication of full-scale behavior; this is despite the fact that the stresses do not arise in the same way as in a field situation. The probable reason is that the high levels of restraint and relatively small testpiece size do lead to stress levels approaching yield (as in the field) and that these do arise within a similar time-scale (within minutes after welding).

Suzuki (Ref. 30) has suggested that the intensity of restraint in a slot specimen is approximately 500 h N/mm mm (where h is the specimen thickness in mm). This is equivalent to a restraint stress in the weld of approximately 261 to 377 N/mm^2 (37.9 to 54.7 ksi) in 13.72 mm (0.54 in.) thick material (Ref. 31). Stress concentrations at the root of incompletely penetrated slot specimens will promote yield stress levels in this region.

In WIC restraint tests the intensity of restraint has been estimated to be 1600 h N/mm mm (where h is the specimen thickness in mm). This would lead to general stress levels in the weld well beyond yield. Also, the WIC cracking test involves a holding time of approximately 16 h prior to sectioning; these two factors might explain why preheat temperatures are higher than those in slot testing (with a 5 min holding time) or in full-scale testing (with a 15 min holding time).

The Schnadt-Fisco test clearly was unable, in the present work, to provide adequate restraint. The degree and reproducibility of restraint in this test have been questioned previously (Ref.

Table 13—Comparison of Critical Preheating Temperatures in Full-Scale and Laboratory Weldability Testing

Steel code	Critical preheat temperature, °C: Full-scale testing	Slot testing	Schnadt-Fisco testing	WIC-restraint testing	Implant testing[a]
3A	100	93	35	125	150
45	75	66	22	100	120
57	75	66	35	–	150

[a]Preventing specimen rupture in a 1000 min holding period.

32); it is concluded that this is not a suitable test for our purposes.

The implant test, with its extremely sharp notch, long time under load (and, in this case, external applied stress equal to yield) is an extremely severe test for HAZ cracking. It is not possible at the present time to determine from first principles the implant rupture strength which will guarantee acceptable field performance; taking a value not less than the nominal yield strength can only be considered a "best guess" estimate for severely stressed joints (Ref. 33). In fact, this criterion can be expected to be overly conservative relative to the HAZ unless loading times are reduced; on the other hand, it does not address the weld metal at all.

The implant test remains an excellent method of ranking the susceptiblity (only) of different materials to HAZ cracking. The clear and logical relationships between cracking stress, chemical composition and hardness emerging from the more broadly-based implant study (Figs. 12 and 13) confirm this belief, and widely applicable relationships of this kind have recently been deduced by Lorenz and Düren (Ref. 7).

Such information does little, however, to remove the perplexities which remain when it is realized that, with high-strength electrodes, it may well be the weld metal which dominates cracking behavior. The fact that, even in the slot test, there is a general relationship between P_{CM} and critical preheat, regardless of electrode type and location of cracking (Fig. 10) is perhaps indicative of the importance of dilution effects. However, much more work is needed on the relationship between the cracking behavior and chemical composition of weld metals.

In any event, it is far from clear that the ultra-low carbon, crack-resistant steels which are beginning to be offered (Ref. 18) will provide any significant advantages, unless corresponding advances are made in the area of consumables. It has been suggested that the use of low-strength E6010 electrodes for all root-pass welding could represent a simple expedient for the elimination of cracking; given the low susceptibility of current steels to HAZ cracking, this could be so. If the deposited metal is too soft, however, it may promote cracking by aggravating the local strain concentrations.

Conclusions

Theoretical stress analysis of the pipe manipulations inherent in stovepipe welding has shown that relatively moderate lifting between root and hot-pass deposition can lead to high local levels of stress in the toe region of the root bead. Misalignment (high-low) considerably aggravates the stress concentrations in this region, as does a decrease in root pass thickness or an increase in wall thickness.

Full-scale weldability tests under very severe conditions (continuous loading for 15 min) showed that a typical Grade 483 material was resistant to cracking up to very high levels of general stress, in the absence of misalignment. When a severe, but not unrealistic, misalignment condition was induced, a preheat temperature in the range of 90 to 100°C (194 to 212°F) was necessary to eliminate cracking at moderate levels of applied stress. The site of crack initiation appeared to be the weld toe, as predicted by theory.

Two other steels gave critical preheat temperatures around 75°C (167°F) under the same conditions. These results are in good agreement with the practical observation that preheats in the region of 100°C (212°F) are sufficient to eliminate cracking problems under virtually all field conditions, using similar materials.

Of the various laboratory weldability tests investigated, those which correlated best with full-scale behavior were the self-restrained slot weld and WIC restraint cracking tests. Cracking percentage was not considered to be a sufficiently reproducible weldability criterion, but critical preheats to prevent cracking in these tests correlated well with the full-scale values.

The implant test can give good indications of the susceptibility of a material to HAZ cracking. However, the relevance of such results must be questioned, particularly in situations where cracking occurs mainly in the weld metal. Much more work is needed on the relationship between cracking and chemical composition in weld metals. The Schnadt-Fisco test was found to be insufficiently severe for the present purposes.

In Canada and elsewhere, owners and contractors are turning more and more towards mechanized (and ultimately, it may be hoped, automatic) GMA welding for major projects involving large-diameter pipe. Because of the low hydrogen content of the weld metal deposited by such processes, the problems addressed in this paper may be expected to assume a steadily-diminishing importance in the future. However, many projects will continue to be welded worldwide by the stovepipe technique. As long as this continues to be so, welding engineers should be aware that insistence on an adequate level of preheat is their most effective protection from the economic and quality penalties associated with a high incidence of cracking.

References

1. Yurioka, N.; Ohshita, S.; and Tamehiro, H. 1981. Study of carbon equivalents to assess cold cracking tendency and hardness in steel welding. Australian Welding Research Association, Specialist Symposium. *Pipeline welding in the 80's.* Paper 1C.

2. Fletcher, L., and Cotton, H. C. 1981. The selection of line pipe for weldability. Australian Welding Research Association, Specialist Symposium. *Pipeline welding in the 80's.* Paper 1D.

3. Phelps, B. 1977. *Microalloying 75*, p. 570, New York: Union Carbide Corporation.

4. Prosser, K., and Cassie, B. A. 1981. Field welding and service experience with gas transmission pipelines. *Steels for line pipe and pipeline fittings conference.* London: The Metals Society.

5. Graville, B. A. 1975. *Cold cracking control.* Montreal: Dominion Bridge Company.

6. Rothwell, A. B., and Bonomo, F. 1977. Weldability of HSLA steels in relation to pipeline field welding. *Welding of line pipe steels.* New York: Welding Research Council.

7. Lorenz, K., and Düren, C. 1981. Carbon equivalent for evaluation of weldability of large diameter pipe steels. *Steels for line pipe and pipeline fittings conference.* London: The Metals Society.

8. Düren, C., Müsch, H., and Wellnitz, G. 1976. Special experiences with vertical-down welding of pipelines in the field. *Proc. welding of HSLA (microalloyed) structural steels conference*, p. 772. Metals Park: American Society of Metals.

9. Mercer, W. L., and Needham, D. 1969. *Welding and inspection of high pressure gas transmission pipelines.* British Gas Council Research Association report GC158.

10. Lumb, R. F., and Fearnehough, G. D. 1975. Towards better standards for field welding of gas pipelines. *Welding Journal* 55 (2):62-s to 71-s.

11. Bufalini, P.; Cerquitella, A.; and DeVito, A. 1980. Tests designed for the assessment of hydrogen-induced cracking in field welding of line pipe. *Welding of pipeline steels conference.* London; The Welding Institute.

12. Higdon, H. J. 1978. *Field weldability of pipeline girth welds.* MASc dissertation. Waterloo, Ontario: University of Waterloo.

13. Weickert, C. A. 1980. *Analysis of detailed geometry of root pass welds of pipelines.* MASc dissertation. Waterloo, Ontario: University of Waterloo.

14. North, T. H.; Rothwell, A. B.; Ladanyi, T. J.; Pick, R. J.; and Glover, A. G. 1981. Full-scale weldability testing of high strength line pipe. *Steels for line pipe and pipeline fittings conference.* London: The Metals Society.

15. Higdon, H. J.; Weickert, C. A.; Pick, R. J.; and Burns, D. 1980. Root pass stresses in pipeline girth welds due to lifting. *Proc. WIC pipeline and energy plant piping conference.* Toronto: Pergamon Press.

16. Ito, Y.; Nakanishi, M.; and Komizo, Y. 1979. Hydrogen delayed cracking in low alloy high strength steel weldments. *Sumitomo search* 22 (11).

17. Sumitomo Metals Industries. 1980. *Sumitomo low P_{CM} line pipe steels for easy welding.* Pub. 800C2120.

18. Sumitomo Metal Industries Ltd. 1980. *Ultra low carbon line pipe steel.* Pub. 800C2121.

19. Gordine, J. 1977. The weldability of some Arctic-grade line pipe steels. *Welding Journal* 56 (7): 200-s to 210-s.

20. Vasudevan, R.; Stout, R. D.; and Pense, A. W. 1980. A field weldability test for pipeline steels—part II. *Welding Journal* 59 (3): 76-s to 84-s.

21. Dittrich, S. 1974. *Schnadt-Fisco weldability test.* IIW Doc. XIE/5/74.

22. Sawhill, J. M.; Dix, A. W.; and Savage, W. F. 1974. Modified implant test for studying delayed cracking. *Welding Journal* 53 (12): 554-s to 560-s.

23. Neumann, V., and Schonherr, W. 1978. The hydrogen induced cold cracking susceptibility of two high strength low alloy steels evaluated by the implant test. *DVS Berichte* (52): 217-223 (Riecansky technical translation).

24. Banks, E. E., and Gunn, K. W. 1978. Australian experience in the welding of cerium-treated C-Mn-Cb steels for structural and pipeline usage. *Proc. ASM conference welding of HSLA (Microalloyed) structural steel*, p. 467, Metals Park: American Society for Metals.

25. United States Steel Corporation. 1978. *Evaluation of the Lehigh slot test weld.* Report, bulletin.

26. Mannesmann, 1979. *Examination of the slot test.* Duisburg.

27. Church, A. K.; Blackwood, E.; and Gunn, K. W. 1980. Evaluation of the Stout slot weld test for pipeline steels. *Australian Welding Journal* (3): 25-31.

28. Hensler, J. H. 1980. Cold cracking comparison of several types of X65 line pipe. *Australian Welding Journal* (5): 5-7.

29. Kaarpi, R. A. J. 1978. *HAZ hardness and carbon equivalents predicting the implant fracture strength.* IIW document IX-1102-78.

30. Suzuki, H. 1980. *Cold cracking and its prevention in steel welding.* Nippon Steel Corporation report; also IIW Doc IX-1157-80.

31. Satoh, K., and Veda, S. 1976. *Studies of structural restraint severity relating to weld cracking in Japan.* IIW document X-808-76 (IX-986-76).

32. Lazor, R. 1982. *Prediction of weld cracking susceptibility.* Welding Institute of Canada report RC-76.

33. Christensen, N., and Simonsen, T. 1981. Assessment of weldability by the implant method. *Scandinavian Journal of Metallurgy,* (10): p 120-126.

34. Pick, R. J.; North, T. H.; and Glover, A. G. 1982. Full-scale weldability testing of large diameter line pipe. *Canadian Mining and Metallurgical Bulletin* 75 (1):122-127.