IRRADIATION EFFECTS IN LOW-ALLOY REACTOR PRESSURE VESSEL STEELS
(HEAVY-SECTION STEEL TECHNOLOGY PROGRAM SERIES 4 AND 5)*

R. G. Berggren, J. J. McGowan, B. H. Menke,†
R. K. Nansstad, and K. R. Thoms‡

Metals and Ceramics Division
OAK RIDGE NATIONAL LABORATORY
Oak Ridge, Tennessee 37831

INTRODUCTION

Numerous studies of the effects of impurities and fast neutron irradiation on fracture toughness of nuclear reactor pressure vessel materials (plates, forgings, and welds) have been reported in the literature.1-9 Most such studies have included a minimum number of tests for each material and/or combination of irradiation parameters. Statistical analysis of neutron irradiation effects has been possible only by using a large body of data representing a variety of materials and neutron exposure conditions. However, such analytical methods do not address the question of accuracy of each datum in the large set, or the accuracy and reliability of results from a single small data set such as might be obtained in a nuclear reactor pressure vessel surveillance study. The present studies address this question by multiple testing at two laboratories of typical nuclear pressure vessel materials (both irradiated and unirradiated) and statistical analyses of the test results. Multiple tests are conducted at each of several test temperatures for each material, standard deviations are determined, and results from the two laboratories are compared.

The Fourth Heavy-Section Steel Technology (HSST) Irradiation Series, almost completed, was aimed at elastic-plastic and fully plastic fracture toughness of low-copper weldments ("current practice welds"). A typical nuclear pressure vessel plate steel was included for statistical purposes.

The Fifth HSST Irradiation Series, now in progress, is aimed at determining the shape of the \( K_T R \) curve after significant radiation-induced shift of the transition temperatures. This series includes irradiated test specimens of thicknesses up to 100 mm and weldment compositions typical of early nuclear power reactor pressure vessel welds.

These two series will be discussed separately.


†Materials Engineering Associates, Lanham, Maryland.
‡Engineering Technology Division, Oak Ridge National Laboratory.
FOURTH HSST IRRADIATION SERIES

This irradiation program was conducted on four submerged-arc welds and one plate of A-533 grade B class 1 pressure vessel steel. The welds were made by commercial vendors using current welding practices and contained relatively low copper contents. The target fast neutron fluence was $2 \times 10^{23}$ neutrons/m$^2$ (>1 MeV) and the irradiation temperature was 288°C (550°F). Charpy V-notch, tensile, and fracture toughness tests (1TCS specimens) were conducted. Sufficient numbers of specimens were included in the irradiations to permit statistical analyses of the results.

MATERIALS

The plate material was from a 305-mm-thick plate of ASTM A-533 grade B class 1 manganese-molybdenum-nickel steel produced by Lukens Steel Company for the HSST Program. This plate material was designated HSST plate 02 and portions of it have been used in many investigations. All test specimens were prepared in the transverse (TL) orientation.

All four submerged-arc weldments were made in ASTM A-533 grade B class 1 plate. Two of the submerged-arc weldments were supplied by the Electric Power Research Institute (EPRI) and had been fabricated by Combustion Engineering, Inc. HSST weld 68W was produced as a 178-mm-thick submerged-arc weldment using Linde 0091 flux and 4.8-mm-diam MIL-B-4 (low copper and phosphorus) wire and was designated "CGS" in EPRI studies. HSST weld 69W was produced as a 300-mm-thick submerged-arc weldment using Linde 0091 flux and 4.8-mm-diam MIL-B-4 wire and was designated "CHS" in EPRI studies. Both welds were postweld heat treated 25 h at 621°C.

The other two welds were supplied by Babcock and Wilcox Company. Both welds were produced as 174-mm-thick submerged-arc weldments using Mn-Mo-Ni (SFA-5.23EF2N) filler wire. HSST weld 70W was fabricated using Linde 0124 flux and HSST weld 71W was fabricated using Linde 0080 flux. Both welds were postweld heat treated 48 h at 607°C.

All test specimens were prepared with longitudinal axes transverse to the weld centerline and crack propagation direction parallel to the surface.

The four weldments are considered "current practice" and are of low or medium low-copper content. Two of the welds (EPRI material) also had low-nickel contents.

Chemical compositions of the five materials are presented in Table 1.

MATERIAL IRRADIATION

All irradiations were conducted at two faces of the Bulk Shielding Reactor (BSR) at Oak Ridge National Laboratory (ORNL), a 2-MW pool-type reactor. Thermal shields of 42.5-mm-thick stainless steel were used between the reactor core and the specimen capsules to reduce gamma heating
Table 1. Chemical compositions of plate and submerged-arc welds

<table>
<thead>
<tr>
<th>Material</th>
<th>Composition, wt %</th>
</tr>
</thead>
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<tr>
<td>Plate, A533 grade B class 1 (HSST-02)</td>
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<tr>
<td>Weld HSST-68W, Linde 0091 flux</td>
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</tr>
<tr>
<td>Weld HSST-71W, Linde 0080 flux</td>
<td>0.124</td>
</tr>
</tbody>
</table>

in the irradiation capsules. Each capsule contained 60 ITCS fracture toughness specimens, 80 to 90 Charpy V-notch impact test specimens, and 10 to 20 tensile test specimens in an arrangement shown in Fig. 1.

Fig. 1. BSR-HSST-4 irradiation capsule specimen rack assembly.

Specimen temperatures during irradiation were controlled by a combination of electrical heaters and controlled sweep gas composition (helium and nitrogen). Specimen temperatures during irradiation were controlled at 288 ± 5°C. Irradiation times for the capsules ranged from 4300 to 5400 h.
Complete spatial maps of damage exposure parameter values were determined for each capsule. The dosimetry in the capsules consisted of multiple foil sets and gradient wires. The dosimetry and spectral adjustment procedures are completely presented elsewhere. The values of fluences greater than 1 MeV, 0.1 MeV, and displacements per atom (dpa) were determined for each specimen. Uncertainties for the damage exposure parameters were less than 8% (standard deviation).

**TEST PROCEDURES**

The Charpy V-notch impact testing was apportioned between Materials Engineering Associates (MEA), Lanham, Maryland, and ORNL, Oak Ridge, Tennessee, such that both laboratories tested equivalent specimens insofar as possible.

The impact testing machines at both laboratories were calibrated in conformance with ASTM Standard Method E23, including proof tests using "standardized specimens."

Preliminary tests of both unirradiated and irradiated specimens were conducted to approximate the Charpy transition curve and specific temperatures were selected for the balance of the testing. Specimens were selected such that, insofar as possible, tests at each selected temperature were conducted by both laboratories of specimens that came from adjacent positions in the original plate or weld, adjacent positions in the irradiation capsules, and about the same neutron exposure.

Mean Charpy-V energies of unirradiated specimens and standard deviations were first determined for each selected test temperature for each laboratory and for the combined data set. This permitted determination of any bias between the laboratories and also provided data on scatter of results inherent in the materials and the Charpy test.

Least-squares regressions were then conducted on the data from unirradiated specimen tests using several fitting expressions:

\[
E = A + BT \quad (1)
\]
\[
E = (A + BT) \left\{ 1 + \tanh \left[ \frac{C}{2} (T - D) \right] \right\} \quad (2)
\]
\[
E = (A + BT) \left\{ 1 - \exp \left[ -C(T + 273)D \right] \right\} \quad (3)
\]

where

- \( E \) = fracture energy, J,
- \( T \) = test temperature, °C, and
- \( A, B, C, D \) = parameters determined in the fitting process.

Equation (1) is a bilinear expression that was fitted separately for the transition and upper-shelf regions. Results that were in the lower and upper "knee" temperature regions were deleted for this analysis. Equations (2) and (3) are "sloping upper-shelf" representations of the
Charpy curves. "Flat upper-shelf" representations of the Charpy curves were also obtained by deleting the BT term in Eqs. (2) and (3).

The results of tests on irradiated specimens (energy-temperature pairs) were then converted to transition temperature shifts (ΔT) or upper-shelf energy changes (ΔE), depending on whether the irradiated specimen test was in the transition or upper-shelf region. Transition temperature shifts are the test temperature minus the temperature at which the mean unirradiated curve gives the same energy as was obtained in the test of the irradiated specimen. The upper-shelf energy change was simply the energy from the irradiated specimen tests minus the mean energy for the unirradiated specimen tests conducted at the same temperature. A positive ΔT is an irradiation-induced transition temperature increase and a positive ΔE is a radiation-induced upper-shelf energy increase.

We then attempted to analyze the radiation-induced changes as a power law function of neutron fluence. However, with the maximum neutron fluence range of only ±50%, the scatter of the data was such that a reliable analysis was not obtained. We therefore determined the mean fluence, mean fracture energy, and mean test temperature for transition region and upper-shelf region data for each material and compared these with the unirradiated specimen data to obtain transition temperature and upper-shelf energy changes.

Testing of compact toughness (CT) specimens was performed by the single specimen compliance (SSC) technique with each laboratory testing approximately half the specimens. All compact specimens designated for upper-shelf testing were side-grooved on each side by 10% of the specimen thickness. The SSC technique provides a method to determine the amount of specimen crack extension by means of small unloadings (<15% of maximum load) conducted at regular intervals throughout the test.

Values of the J-integral were calculated using the modified version of the J-integral, known as $J_M$, as proposed by Ernst. $^{15}$ $J_M$ is given by:

$$ J_M = J_D - \int_{a_0}^{a} \frac{a[J_D - G]}{3a} \left| \frac{\delta_{pl}}{a} \right| \, da, \quad (4) $$

where

- $J_D = \text{deformation theory J}$,
- $G = \text{Griffith linear elastic energy release rate} = K_I^2(1 - \nu^2)/E$,
- $a_0, a = \text{the initial and current crack lengths}$,
- $J_D - G = J_{pl}$, the plastic part of the deformation theory J,
- $\delta_{pl} = \text{the plastic part of the displacement}$, and
- $\nu = \text{Poisson's ratio}$.

Deformation theory J ($J_D$) contained in Eq. (4) is the formation of the J-integral specified for use in the ASTM Standard Test for $J_{IC}$, a
A typical R curve produced with the SSC technique is illustrated in Fig. 2. The R curve format of Fig. 2 is in accordance with that of ASTM Standard E813, i.e., $J_{IC}$, the initiation elastic-plastic fracture toughness, is defined by the intersection with the blunting line of the linear regression fit to the data between the 0.15- and 1.5-mm exclusion lines. The use of linear least-squares fit stems from the multispecimen nature of ASTM E813 where a minimum of only four data points are required. Thus, with only a few data points, the nonlinear nature of the R curve cannot be evaluated. For this study, we used the procedure proposed by Loss et al., 17-18 whereby a power law, $J = C(Aa)^N$, is fit to the data between the 0.15- and 1.5-mm exclusion lines with the constants C and N chosen to optimize the curve fit. The initiation fracture toughness, $J_{IC}$, is defined with this procedure as the intersection of the power law R curve with the 0.15-mm exclusion line as indicated in Fig. 2. $J_{IC}$, as defined by the two methods, is nearly identical for nuclear grade pressure vessel steels. In addition to more closely describing the nonlinear behavior of the R curve, the power law $J_{IC}$ definition provides a consistent means for determining the initiation toughness when fast fracture, i.e., brittle fracture by a cleavage micromode, occurs prior to development of a full R curve. To address this cleavage phenomenon, the power law method classifies the R curve into three types: types A, B, and C. With type A, failure occurs before the slow ductile crack extension is sufficient to cross the 0.15-mm exclusion line. In this case, $J_{IC}$ is taken as the value of $J_M$ at failure. Type B covers cases where testing was terminated by fast fracture after ductile crack extension exceeded the 0.15-mm exclusion line, but prior to reaching the 1.5-mm exclusion line. In this case, a power law is fit to the available data and $J_{IC}$ is taken as the intersection of the fit with the 0.15-mm exclusion line. Type C covers cases where the R curve extends beyond the 1.5-mm exclusion line.

![Power law representation of the J-R curve using the SSC test technique and compared with the ASTM E813 procedure.](image)
Another effect resulting from the nonlinear nature of the R curve is that the tearing modulus is not constant as was originally envisioned by ASTM E813. For comparison purposes and consistency with ASTM E813, a single value for the tearing modulus ($T_{avg}$) can be computed from the power law equation for type C R curves. The tearing modulus is defined as:

$$T = \frac{E}{\sigma_f^2} \frac{dJ}{da},$$  \hspace{1cm} (5)

where $\sigma_f = (\text{yield stress} + \text{ultimate stress})/2$.

To obtain an average value for the slope of the power law R curve, the power law equation is fit in closed form with a linear regression of the form $J = J_0 + (dJ/da)\Delta a$ to that part of the curve lying between the exclusion lines. This technique is directly analogous to the E813 method of fitting a linear line to the discrete data points. However, fitting the power law equation has the effect of including an infinite number of points. Little difference in magnitude is observed between the values of $T$ obtained by the two techniques.

The power law and E813 methodologies produce similar results in regions covered by the E813 standard. The procedure is to convert the elastic-plastic $J_{IC}$ power law initiation toughness to a quasielastic $K_{JC}$ value using the following relationship:

$$K_{JC} = [EJ_{IC}]^{0.5}. \hspace{1cm} (6)$$

ASTM Standard E813 gives an equation for converting $J_{IC}$ to $K_{JC}$, which incorporates a plane-strain term. Equation (6) is identical to the E813 equation, but with the plane-strain term removed. This term was removed because in this program specimens tested in the upper transition region and those tested on the upper shelf experienced net section yielding. Such behavior is closer to plane stress than it is to plane strain. In the lower transition region, however, the specimen through-thickness constraint is greater and more nearly plane strain. Thus, it is unclear just where one should use the E813 equation. Since most of the data generated in this study were more nearly plane stress and for consistency of presentation, all $K_{JC}$ values were calculated using Eq. (6).

At toughness levels too great to allow measurement of a valid $K_{IC}$ with a small specimen, $K_{JC}$ values can be determined as stated above; however, these data tend to be of greater magnitude than that which would be obtained with a larger specimen capable of measuring the linear elastic toughness level by E399 criteria. A similar observation was made by Irwin in that the plane stress fracture toughness ($K_C$) overestimates $K_{IC}$. Irwin developed an empirical relationship from which $K_{IC}$ could be estimated once $K_C$ was known. Recently, Merkle has shown that reasonable estimates of $K_{IC}$ can be determined using $K_{JC}$ as a substitute for $K_C$. In this study, $K_{JC}$ values in the brittle-to-ductile transition region were
corrected to K_\beta_c using the Irwin-Merkle \beta_{1c} correction to provide another means of determining the shift to higher temperature of the brittle-to-ductile transition with irradiation.

RESULTS — CHARPY TESTS

Charpy impact test results for unirradiated specimens are summarized Figs. 3 and 4 and Table 2. In Table 2 the results for the two laboratories can be compared. The means for the two laboratories are, in almost all cases, within one standard deviation of each other, indicating good reproducibility. There may be a tendency for the ORNL results to give slightly higher energies in the transition region and MEA results to give slightly higher energies in the upper-shelf region. The standard deviations do not show any consistent trends with laboratory, material, or region of the curve. They do, however, indicate the possible errors that might be encountered if only a few tests are conducted in the transition or upper-shelf regions. In Fig. 3 the results of fitting the data for HSST plate 02 to all five Charpy curve expressions can be seen to give virtually identical transition temperatures and upper-shelf energies. The greatest differences are in the lower- and upper-knee regions where there are few data. In this plate material the upper shelf is very nearly a constant value over a range of 150°C. The results of fitting data for submerged-arc weld HSST 70W are presented in Fig. 4. In this case a positively sloping upper shelf is indicated by the analysis. The transition temperatures, however, were almost identical for all fitting expressions. These examples are typical of most of the analyses. The only exceptions were for a few data sets where the distribution or quantity of data were such that the fitting was affected. In both these figures one can visualize the possible effects on the shape and position of the curves if few tests had been conducted.

Fig. 3. Several functions fitted to Charpy V-notch impact data for unirradiated A-533 grade B class 1 steel plate (HSST-02).
Table 2. Comparison of Charpy impact test results for unirradiated pressure vessel plate and submerged-arc welds from ORNL and MEA

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<tr>
<th>Test temperature (°C)</th>
<th>Mean energy (E), J</th>
<th>Standard deviation (σn), J</th>
<th>Number of tests</th>
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<td>288</td>
<td>137.9</td>
<td>145.9</td>
<td>141.5</td>
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Submerged-arc weld (HSST-88W)

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Submerged-arc weld (HSST-89W)

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Submerged-arc weld (HSST 70W)

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Submerged-arc weld (HSST 71W)

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<td>115.2</td>
<td>107.5</td>
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*aNo tests by MEA.*
As stated earlier, fluences greater than 1 MeV, greater than 0.1 MeV, and dpa were determined. However, in this study it was found that the ratio of fluence greater than 0.1 MeV to fluence greater than 1 MeV was 3.629 with a standard deviation of 0.104 and the ratio of dpa to fluence greater than 1 MeV was $1.616 \times 10^{-23}$ with a standard deviation of $0.024 \times 10^{-23}$. In view of these standard deviations, any effect of exposure parameter was undetectable and test results will be presented in terms of neutrons of energy greater than 1 MeV.

Tensile strength changes, due to irradiation, are given in Table 3 (ref. 23). In Figs. 5 through 8, typical examples are presented of the results of Charpy V-notch impact tests on the irradiated materials. Inherent in the experiment is a fluence variation due to the fast neutron flux gradients at the reactor. The standard deviation of fluence values was about 30% of the mean fluence for each material. Figure 5 presents the results for the material, HSST plate 02, and a hyperbolic tangent [sloping shelf, Eq. (2)] fit for the mean fluence and in Fig. 6 a similar fit for the submerged-arc weld HSST 71W. It can be seen in Figs. 7 and 8 that the amount of scatter in the results is not exclusively a result of fluence differences. Power law functions were fitted to the data in these figures and the best fit equations in these examples were

$$\Delta T = 55.2 (\phi/10^{23})^{0.443} \quad \text{(Fig. 7)},$$

and

$$\Delta T = 33.1 (\phi/10^{23})^{0.206} \quad \text{(Fig. 8)},$$

where

$\Delta T =$ transition temperature increase, °C, and

$\phi =$ fluence, $10^{23}$ n/m², >1 MeV.

Table 3. Average tensile strengths

<table>
<thead>
<tr>
<th>Material</th>
<th>Average yield strength from 22 to 288°C (MPa)</th>
<th>Change (%)</th>
<th>Average ultimate tensile strength from 22 to 288°C (MPa)</th>
<th>Change (%)</th>
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<tbody>
<tr>
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<td>Unirradiated</td>
<td>Irradiated</td>
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<tr>
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<td>430</td>
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The exponents on neutron fluence given in the above equations are within the range of exponents obtained in other studies. However, other exponents were tried in a sensitivity analysis and almost any exponent gave nearly the same standard deviations. Attempts to fit a power law function to the other transition temperature shift data and to the upper-shelf change data yielded exponents that ranged from greater than unity to negative values (concave upward). These results were due to several factors: the scatter in the data, the rather restricted fluence range, and the low sensitivity of these "current practice" welds.

The results are, therefore, presented in Table 4 in terms of mean fluence, mean transition temperature increase, and mean upper-shelf change. The plate material, having the greatest copper and nickel
Fig. 7. Transition temperature increase of A-533 grade B class 1 steel plate (HSST-02) irradiated to 288°C.

Fig. 8. Transition temperature increase of submerged-arc weld metal (HSST-69W) irradiated at 288°C.

contents, had the largest changes in both transition temperature increase and upper-shelf energy decrease. Weld HSST 68W with both low-copper and low-nickel contents showed the smallest transition temperature increase while the other three welds gave intermediate transition temperature increases. Of interest is the observation that welds HSST 70W and 71W with low copper and normal nickel contents had transition temperature increases only slightly less than for weld HSST 69W, which had medium copper and low nickel contents. The upper-shelf energy changes for the four welds are within the observed scatter of the data, and are probably not significant.
Table 4. Effect of fast neutron irradiation on Charpy V-notch impact properties of plate and welds
(All values are means for the data sets)

<table>
<thead>
<tr>
<th>Transition temperature increase</th>
<th>Upper-shelf energy change</th>
</tr>
</thead>
<tbody>
<tr>
<td>Fluence $10^{23}$ n/m$^2$ (&gt;$1$ MeV)</td>
<td>Fluence $10^{23}$ n/m$^2$ (&gt;$1$ MeV)</td>
</tr>
<tr>
<td>Shift ($^\circ$C)</td>
<td></td>
</tr>
<tr>
<td>2.04</td>
<td>68.2</td>
</tr>
<tr>
<td>1.35</td>
<td>6.0</td>
</tr>
<tr>
<td>1.22</td>
<td>34.2</td>
</tr>
<tr>
<td>1.65</td>
<td>30.9</td>
</tr>
<tr>
<td>1.65</td>
<td>26.4</td>
</tr>
</tbody>
</table>

In Table 5 the transition temperature increases obtained in this study are compared to predictions from several trend curve formulations. In making this comparison, several factors must be considered:

1. The predictions of the trend curve formulations are largely or wholly based on surveillance results and represent lower fluxes and much longer irradiation periods than for the present study.

2. The Regulatory Guide 1.99 (ref. 1) was meant to be conservative.

3. The values based on the Draft Regulatory Guide (ref. 2) do not include the "margin" specified in the draft.

4. In several cases the tabulated predicted values are for compositions outside the limits given by the authors.

Considering the above factors, there is fairly good agreement of the present results with the several predictions.
Table 5. Comparison of several radiation embrittlement predictive expressions with results of present study

<table>
<thead>
<tr>
<th>Material</th>
<th>Fluence, (10^{23}) n/m² (&gt;1 MeV)</th>
<th>Transition temperature increase, °C</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td></td>
<td>Observed AT&amp;T</td>
</tr>
<tr>
<td>Plate HSST-02</td>
<td>2.04</td>
<td>68</td>
</tr>
<tr>
<td>Weld HSST-68W</td>
<td>1.35</td>
<td>6</td>
</tr>
<tr>
<td>Weld HSST-69W</td>
<td>1.22</td>
<td>34</td>
</tr>
<tr>
<td>Weld HSST-70W</td>
<td>1.65</td>
<td>31</td>
</tr>
<tr>
<td>Weld HSST-71W</td>
<td>1.65</td>
<td>26</td>
</tr>
</tbody>
</table>

ᵃRef. 1, Regulatory Guide 1.99.<br>ᵇRef. 2, Draft Regulatory Guide 1.99.<br>ᶜRef. 3, Varsik and Byrne.<br>ᵈRef. 4, Perrin, Wullaert, Odette, and Lombrozo.<br>ᵉRef. 5, Varsik, Schloss, and Koziol.<br>fReg. 6, Guthrie.<br>Outside limits of applicability per author.

RESULTS — FRACTURE TOUGHNESS TESTS

All side-grooved specimen J-R curves obtained in this study were valid by ASTM E813 criteria and the agreement between MEA and ORNL data, both in the transition and upper-shelf regimes, is excellent. J-R curves obtained from tests at 200°C are illustrated in Fig. 9 in the tearing instability format. The solid curves result from the power law curve fit plotted in terms of \(J_{lc}\) and \(T_{mat}\). The data symbols indicate crack extension at \(J_{lc}\) (lower right), 0.5, 1, 1.5, and 2 mm, respectively. Extrapolation to larger values is not appropriate since questions remain as to the validity of the data beyond approximately 2 mm of crack extension.

It is clear that for each material the J-R curves are generally lowered by both irradiation and increased test temperature. Note that both the J level, and to a greater extent the tearing modulus, are lowered.

The effect of temperature and irradiation on initiation toughness and tearing modulus is illustrated directly in Figs. 10 and 11, respectively. All data for a given material condition were fit with a linear regression line. Only the line is plotted for clarity. In general, both \(J_{lc}\) and
Fig. 9. Effect of irradiation on J-R curve obtained from 200°C tests.

Fig. 10. Variation of initiation fracture toughness, $J_{IC}$, with test temperature and irradiation (upper-shelf data).

Fig. 11. Variation of tearing modulus, $T$, with test temperature and irradiation (upper-shelf data).
T_{avg} decrease with increasing temperature and with irradiation; however, the tearing modulus is more sensitive to irradiation. This is evidenced by considering the average change indicated by each parameter for a given material and condition. For instance, the initiation toughness of plate 02, and welds 69W and 71W drop only slightly while the tearing modulus for these same materials drops substantially. The inconsistent behavior evidenced by the unirradiated \( J_{IC} \) line for weld 68W is believed to be due to data scatter.

The fracture toughness behavior in terms of the quasielastic parameter \( K_{JC} \) is illustrated in Figs. 12 through 14. In these figures, the solid line trend curves are the result of regression curves fit to all data points. On the upper shelf, a linear regression was used and in the transition the data were fit with an exponential of the form \( K_{JC} = A[\exp(B \cdot \text{temperature})] \) with the constants \( A \) and \( B \) chosen to optimize the fit. All shifts in transition temperature were calculated using the exponential curve fit equations.

Data for plate 02 material are depicted in Fig. 12. This material had the highest levels of both copper (0.14%) and nickel (0.67%) and the \( K_{JC} \) data indicate the greatest shift in the transition temperature. There is not, however, a corresponding large change in upper-shelf toughness. Data scatter is low for this material, as would be expected in a homogeneous reference material such as plate 02.

![Fig. 12. Variation of initiation fracture toughness, \( K_{JC} \), of HSST plate 02 material with test temperature and irradiation.](image)

Weld 68W had the lowest copper (0.04%) of all five materials and a low nickel content (0.13%). As illustrated in Fig. 13, this material also showed the least radiation effects. The shift in transition temperature (~6°C) is almost insignificant when compared with that for plate 02.

Figure 14 illustrates the behavior indicated for weld 71W. This weld had essentially the same copper level (0.046%), but a higher nickel level (0.63%) than weld 68W. The transition shift is relatively small compared with that for plate 02, but greater than for weld 68W.
The shift in transition temperature for all five materials is summarized in Fig. 15. In this figure, the shift indicated by Charpy V-notch test results can be directly compared with that of $K_{JC}$ and $K_{BC}$. The CVN shift was indexed at 41 J and $K_{JC}$ was indexed at 125 MPa m. $K_{BC}$ was indexed at 90 MPa m for all five materials. This $K_{BC}$ level roughly corresponds to the $\beta_{IC}$-corrected 125 MPa m $K_{JC}$ index for all five materials. Comparison of the CVN shift with $K_{JC}$, and $K_{BC}$ indexed at 90 MPa m give similar results since the unirradiated and irradiated $K_{JC}$ and $K_{BC}$ transition curves are essentially parallel.

It is clear in Fig. 15 that the transition temperature shift is reduced by decreasing both the copper and the nickel content. Reducing either one separately does not produce as substantial a change in the shift as reducing both. Only the welds can be compared directly. The highest shift is evidenced by the data from weld 69W with the highest copper and lowest nickel, thus indicating that copper alone has a substantial effect. However, increased nickel with lowered copper
(welds 70W and 71W) also produces a substantial but lesser shift. The lowest shift was produced in weld 68W where the percentage of both elements was reduced. Although the magnitude of the transition temperature shift varies somewhat for each indicator, all three support these observations.

The results of this irradiation effects study are summarized in Table 6. Primarily, the J-integral results have indicated that reduced levels of copper and nickel will reduce the effect of radiation on the behavior of the brittle-to-ductile transition and will reduce the degradation of the upper-shelf tearing modulus. Significant decreases (>25%) in the tearing modulus were observed in all materials with either copper or nickel contents above those of the weld having the lowest levels of these elements. Only when both elements were reduced was the tearing modulus degradation reduced. In terms of initiation fracture toughness, \( J_{IC} \), the upper-shelf level was essentially insensitive to all variables, i.e., radiation, copper content, and nickel content appeared to have little or no effect. The transition region, on the other hand, was substantially more sensitive to these variables. Variations were observed that appear to be directly related to changes in either the copper or nickel content. Only when both elements were reduced was the shift in the \( K_{JC} \) brittle-to-ductile transition minimized.

Another observation is made regarding the shape of the \( K_{JC} \) transition curve before and after irradiation. When either small or large shifts occurred, the shape did not appreciably change. This observation is significant since estimates of the \( K_{IC} \) shift currently are based on the assumption that there is no shape change. Historically, the CVN transition curve shape has changed with shifts to higher temperature. In this study with ITCS specimens, the data indicate that the fracture toughness, \( K_{JC} \), curve does not follow this pattern.
Charpy V-notch data, in general, indicated transition temperature shifts that agree well with the shifts from the fracture toughness tests. The CVN upper-shelf changes also agree well with that indicated by the fracture toughness data, except for the case of plate 02. For plate 02, Charpy data indicated a moderate shelf drop, as did the initiation fracture toughness data; however, the tearing modulus from the J-R tests dropped by roughly 50%.

Additional details on this irradiation series may be found in refs. 25 and 26. Additional statistical analyses will be made when the final tests are completed.

CONCLUSIONS

1. There was excellent agreement between the results from the two laboratories, ORNL and MEA.

2. All five Charpy curve fitting equations gave nearly identical transition temperatures and transition temperature shifts.

3. Upper-shelf energy determinations should consider a possible sloping shelf.
4. A small data set can lead to erroneous conclusions regarding irradiation-induced changes of both transition temperature and upper-shelf energy.

5. The several trend curve formulations were in general agreement with Charpy specimen results of the present study.

6. Upper-shelf elastic-plastic initiation fracture toughness is essentially insensitive to radiation damage for the current production practice welds characterized in this study.

7. Transition temperature shifts to higher temperature with radiation are directly related to the copper and nickel content of the material with copper producing the greatest effect.

8. Upper-shelf initiation fracture toughness and tearing modulus are inversely proportional to temperature.

9. Irradiation degradation in tearing modulus is related to the copper and nickel content of the material.

10. Transition temperature shifts indicated by the CVN data indexed at the 41-J level correlate well with the fracture toughness indicated shift for these materials.

11. Submerged-arc welds with both low copper and low nickel contents show essentially zero radiation embrittlement.

**FIFTH HSST IRRADIATION SERIES**

The primary objective of this program (Fifth HSST Irradiation Series) is to obtain valid fracture toughness \( K_{IC} \) curves to as a high a level as practical for two nuclear pressure vessel materials irradiated at 288°C.

Currently, estimates of the \( K_{IC} \) curve shift are based on results from Charpy impact testing with the assumption that the shift of a Charpy toughness curve to higher temperatures can be applied directly to a \( K_{IC} \) curve. To test this assumption, this program includes Charpy V-notch impact test specimens and drop-weight test specimens in addition to the compact fracture toughness specimens. Tensile specimens are included in the program to provide data for determining test parameters for the fracture toughness tests and for analysis of the fracture toughness data.

Irradiation and material parameters were chosen to (1) provide significant separation of unirradiated and irradiated properties (i.e., a significant radiation-induced temperature shift of toughness properties), (2) represent, as closely as practical, materials used in early nuclear pressure vessel construction (high copper and nickel contents), and (3) permit program conclusion in a reasonable time period. The chosen irradiation parameters are an irradiation temperature of 288°C (550°F) and a neutron fluence of \( 1.5 \times 10^{23} \) neutrons/m² (>1 MeV). The chosen materials are submerged-arc weldments of 0.23 and 0.32% Cu content.
(0.60% Ni in both weldments). The predicted temperature shifts of fracture toughness for these two weldments are 86 and 121°C, respectively, for the above irradiation parameters. The lower bounds (95% confidence level) are 58 and 93°C, respectively. These predictions are based on the Metal Properties Council analyses\(^2\) of weld metals irradiated in test reactors.

The program plan provides sufficient numbers of specimens to permit meaningful statistical analyses of test results.

MATERIALS

In order to provide as uniform a test material as possible, submerged-arc weldments were fabricated using two special heats of AWS type EF-2 welding wire with copper added in the ladle to achieve the two levels of copper content. One lot, well mixed, of Linde 124 flux was used for all the welds. The weldments were fabricated and stress-relieved according to commercial practice. The base plate for the weldments is a single 220-mm-thick plate of SA-533 grade B class 2 steel. The submerged-arc welding was done by the tandem-arc, alternating-current procedure using a 0° bevel weld groove. The width of the deposited weld metal is about 30 mm. All welds were stress-relieved at 607°C for 40 h. About 14 lin m of each weldment were fabricated for the program.

SPECIMEN COMPLEMENT

The largest practical compact (\(K_I\)) specimen that can be irradiated is a 4TCS specimen. The irradiated yield stress will be about 620 MPa (90 ksi), resulting in a valid fracture toughness (\(K_I\)) measuring capacity of 130 MPa√m (120 ksi√in.). To achieve this toughness level in the unirradiated specimens, having a yield stress of about 480 MPa (70 ksi), requires 8TCS specimens and these are provided for in the program. Smaller fracture toughness specimens are provided for measuring toughness at lower levels, obtaining \(K_J\) values for predicting test parameters for the larger specimens, and obtaining data for comparisons of \(K_I\) and \(K_J\) results. A series of irradiation capsules will contain a total of 16 each 4TCS, 36 each 2TCS, 60 each 1TCS, 112 each Charpy-V, 32 each drop-weight, and 28 each subsize tensile specimens, divided equally between the two materials. Sufficient numbers of unirradiated specimens will be tested to provide base line properties. The numbers of specimens are based on consideration of statistical analysis requirements and the constraints of the irradiation facility. Sufficient weldments are available for fabrication of additional unirradiated specimens, if found necessary.

IRRADIATION CAPSULE DESIGN AND OPERATION

The Fifth HSST Irradiation Series consists of a total of 12 capsules to be irradiated in the poolside facility of the Oak Ridge Research Reactor (ORR). Neutron dosimetry studies in the facility were conducted in a dummy capsule and a prototype of the 4T capsules was operated in the facility to obtain neutron and gamma heat parameters for final experiment
design. A steel gamma shield was incorporated into the facility to allow control of specimen temperatures. Neutron dosimeter sets are included in each capsule to verify the exposures.

The first two 4T capsules were installed in the irradiation facility and, following extensive checkouts of both the capsules and the facility instrumentation, the irradiation facility was inserted and irradiation began on May 11, 1984. The desired 288°C irradiation temperature was maintained at the quarter-thickness points at the specimen crack fronts. Due to the heat flow patterns in these specimens, temperatures at the center of the crack fronts were 2 to 3°C higher and at 3 mm from the ends of the crack front were 7 to 12°C lower than at the quarter-thickness points. This temperature gradient is due to the gamma heating in these massive specimens and would have been greater except for the use of steel gamma shields. (We are able to orient the small specimens to reduce the temperature gradients along the crack front to very small values.)

After 30 equivalent full-power days of irradiation, the capsules were rotated on June 11, 1984, and, after an additional 30 days, the irradiation was terminated on July 21, 1984. The temperature control during irradiation was very good. The desired 288°C at quarter-thickness position was easily maintained. The recorded temperature data are presently being reduced and average temperatures for all thermocouples, as well as their variation with time, will be reported in the next HSST semiannual progress report.

The second set of 4T capsules began irradiation on August 16, 1984. The assembly of the small specimen (1TCS, Charpy, drop-weight, and tensile) capsules is almost complete and preparations for the 2T capsules are well under way.

TEST PLAN

Two organizations, Materials Engineering Associates, Inc., (MEA) and Oak Ridge National Laboratory (ORNL) will participate in the testing program. Because of testing equipment limitations, all testing of irradiated 4TCS specimens and unirradiated 6TCS and 8TCS specimens will be conducted by MEA and all tensile tests will be conducted by ORNL. The sequence of testing will be:

1. materials characterization,
2. testing of unirradiated Charpy V-notch, tensile, and drop-weight specimens,
3. testing of unirradiated compact specimens, smaller specimens being tested first, and $K_{JC}$ tests preceding the large specimen tests,
4. testing of irradiated Charpy V-notch, tensile, and drop-weight tests,
5. testing of irradiated compact specimens, smaller specimens being tested first, and $K_{JC}$ tests preceding the large specimen tests, and
6. statistical analyses of results.
ACKNOWLEDGMENTS

We wish to thank M. Vagins of the U.S. Nuclear Regulatory Commission for supporting this study. We appreciate the assistance of the Electric Power Research Institute and Babcock and Wilcox Company in providing the weldments for the Fourth Series. We also wish to thank F. B. Kam and his co-workers for the dosimetry analyses, J. W. Woods and D. Heatherly for construction and operation of the Fourth Series irradiation capsules, technicians T. N. Jones, T. D. Owings, and R. L. Swain for conducting the tests at ORNL, and J. R. Hawthorne, A. Hiser, E. D'Ambrosio, L. Fletcher, and L. Lamont for conducting the tests at MEA. We thank J. L. Bishop for preparation of the manuscript.

REFERENCES


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