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**Heavy-Section Steel Technology Program
Quarterly Progress Report for
January-March 1976**

G. D. Whitman

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OAK RIDGE NATIONAL LABORATORY

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**HEAVY-SECTION STEEL TECHNOLOGY PROGRAM QUARTERLY
PROGRESS REPORT FOR JANUARY-MARCH 1976**

G. D. Whitman

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FOREWORD

Progress reports on programs conducted at Oak Ridge National Laboratory (ORNL) under sponsorship of the Nuclear Regulatory Commission's Division of Reactor Safety Research are published quarterly. The programs include the Light-Water-Reactor Safety Programs, the Heavy-Section Steel Technology Program, Liquid-Metal-Cooled Fast Breeder Reactor Safety Programs, and the High-Temperature Gas-Cooled Reactor Program.

This report covers work performed by ORNL as well as subcontractors at other installations on the Heavy-Section Steel Technology Program.

PREFACE

The Heavy-Section Steel Technology (HSST) Program, a Nuclear Regulatory Commission (NRC)-sponsored engineering research activity, is devoted to extending and developing the technology for assessing the margin of safety against fracture of the thick-walled steel pressure vessels used in light-water-cooled nuclear power reactors. The program is being carried out in close cooperation with the nuclear power industry. This report covers HSST work performed from January to March 1976, except for subcontractor contributions which may cover the three-month period ending in February. The ORNL work and subcontracts are managed by the Reactor Division. Major tasks at ORNL are carried out by the Reactor Division and the Metals and Ceramics Division. Prior progress reports on this program are ORNL-4176, ORNL-4315, ORNL-4377, ORNL-4463, ORNL-4512, ORNL-4590, ORNL-4653, ORNL-4681, ORNL-4764, ORNL-4816, ORNL-4855, ORNL-4918, ORNL-4971, ORNL/TM-4655 (Vol. II), ORNL/TM-4729 (Vol. II), ORNL/TM-4805 (Vol. II), ORNL/TM-4914 (Vol. II), ORNL/TM-5021 (Vol. II), ORNL/TM-5170, and ORNL/NUREG/TM-3.

SUMMARY

1. PROGRAM ADMINISTRATION AND PROCUREMENT

The Heavy-Section Steel Technology Program is an engineering research activity being conducted by the Oak Ridge National Laboratory (ORNL) for the Nuclear Regulatory Commission in coordination with other research sponsored by the federal government and private organizations. The program comprises studies related to all areas of the technology of the materials fabricated into thick-section primary-coolant containment systems of light-water-cooled nuclear power reactors. The principal area of investigation is the behavior and structural integrity of steel pressure vessels containing cracklike flaws. Current work is organized into the following tasks: fracture mechanics analyses and investigations, effect of high-temperature primary reactor water on the subcritical crack growth of reactor vessel steel, investigations of irradiated materials, pressure vessel investigations, and thermal shock investigations, and weld-heat-affected-zone cracking. Experimental work on the detection of weld-heat-affected-zone cracking has been suspended for the remainder of the current fiscal year. Further assessment of this phenomenon has been deferred until the transition quarter of the fiscal year and no report is included on this subject.

Progress on work under the four existing research and development subcontracts is included in this report.

During this quarter, 11 briefings or presentations were made; a research and development subcontract with the Virginia Polytechnic Institute was initiated; and several blocks of HSST plate material were sent to researchers of the International Atomic Energy Agency, the Naval Research Laboratory, and the Federal Republic of Germany.

2. FRACTURE MECHANICS ANALYSES AND INVESTIGATIONS

An approximate closed-form solution for the stress-intensity factor for a continuous inside surface circumferential crack in a cylinder under thermal loading has been developed. The numerical results of this analysis compare satisfactorily with finite-element calculations. In addition, a

procedure for applying the Irwin equation to relatively shallow continuous surface cracks is presented so as to avoid division by small numbers. Finally, a closed-form expression for the Irwin equation is developed for application to shallow continuous surface cracks subjected to linearly varying nominal stress distributions.

3. EFFECT OF HIGH-TEMPERATURE PRIMARY REACTOR WATER ON THE SUBCRITICAL CRACK GROWTH OF REACTOR VESSEL STEEL

Testing of the first fatigue specimen of A508 forging material at low cyclic frequency was completed. The data show no difference in behavior between forging and plate materials. Testing was started on a specimen of weld material. Testing at 0.1 cpm of specimen 0213-7 is continuing. A computer program has been completed for data interpretation by the ASTM-recommended incremental polynomial method.

4. INVESTIGATIONS OF IRRADIATED MATERIALS

Dynamic testing of the 101.6-mm-thick (4-in.) 4T compact-tension (CT) weld specimens was completed. The weld metal exhibited dynamic fracture toughness of $>236 \text{ MN}\cdot\text{m}^{-3/2}$ ($>215 \text{ ksi}\sqrt{\text{in.}}$) at 74°C (165°F).

The experiment for irradiating 4T-CT specimens of submerged-arc weldments is on schedule, with irradiation in the Bulk Shielding Reactor expected to begin in late May.

The use of epoxy resins for joining irradiated pieces of steel to unirradiated grip portions appears to be feasible for 2T- and 4T-CT specimens.

Small specimen fracture toughness on the upper shelf of unirradiated ASTM A533-B was investigated using the R -curve technique and a crack initiation technique to determine J_{IC} . The R curve results verified the results of J_{IC} as determined by each individual specimen; J_{IC} fracture toughness compared closely to K_{IC} results from large specimens.

5. PRESSURE VESSEL INVESTIGATIONS

Tests are being planned for the remaining intermediate test vessels (V-7A, V-8, and V-10). The repaired vessel (V-7A) is being instrumented for retesting under conditions identical to the initial test except that loading will be sustained by pressurization with nitrogen. Plans are being made to repair this vessel again using the Section XI weld repair procedure without stress relief and to test the vessel (V-7B) again with a sharp crack in the repair zone.

Residual stress measurements in the weld procedure qualification piece (the V-9 prolongation) for the V-7 repair are being made. The weld metal from 31.8- to 38.0-mm-thick (1 1/4 to 1 1/2-in.) test coupons has been characterized.

Studies are continuing in preparation for sustained-load testing of vessel V-8. This vessel will be used for crack-propagation and -arrest demonstration tests. Theoretical studies indicate the feasibility of such tests. As with all previous intermediate vessel tests, model vessels will be tested to assure that analytical methods are adequate and to establish the proper test specifications.

6. THERMAL SHOCK INVESTIGATIONS

A core containing the entire flaw in TSV-2 was trepanned and examined. Scanning electron microscopic examination shows the fracture surface to be cleavage. Additional calculations were made in connection with interpretation of TSE-2; additional fracture toughness measurements of TSV-2 material were made; a proposal was made for two additional experiments; stress-intensity factors were calculated for a continuous circumferential flaw in the reference calculational model (PWR); and the thermal shock test facility was modified to reduce the asymmetry in the quenching of test cylinders. A further examination of the results of TSE-1 indicates that the crack extension observed in that test was subcritical crack growth under combined residual and thermal stresses.

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1. PROGRAM ADMINISTRATION AND PROCUREMENT

G. D. Whitman

The Heavy-Section Steel Technology (HSST) Program is a major Nuclear Regulatory Commission (NRC)-sponsored safety program concerned with the structural integrity of the primary systems, particularly the reactor pressure vessel, of water-cooled nuclear power reactor stations. Practically all areas of materials technology relating to the steels and weldments used in fabricating the vessels of present and contemplated plants in the United States have been or are being investigated. Extensive studies have been made and are continuing on the metallurgical, mechanical, and toughness properties exhibited by typical reactor vessel product forms. Quantitative techniques have been developed for determining toughness properties for use in analytical methods for predicting structural behavior considering the presence of flaws. Plates, forgings, and weldments are being investigated, including the effects of cyclic crack propagation, environmental corrosion, and irradiation.

One of the major objectives of the HSST program is to quantitatively relate the flaw size (under a specified constraint condition), load, and toughness at which fracture will occur over the temperature range of interest for water-cooled reactors [room temperature to around 288°C (550°F)]. Such a relationship would allow the margin of safety against fracture of a structure to be calculated. Currently, major emphasis is on the development of a technology that is capable of making quantitative engineering calculations of fracture conditions utilizing parameters that have been obtained by accurate or conservative procedures. Such a technology provides for the assessment of the potential of fracture, burst, or leakage of water-cooled reactor pressure vessels as well as other structural components of the primary containment system during all phases of normal operation and under conditions imposed by accidents.

The program is coordinated with efforts of other government agencies and the manufacturing and utility sectors of the nuclear power industry both in the United States and abroad. The overall objective is a quantification of safety assessments that are needed by the regulatory agencies,

the professional code-writing bodies, and the nuclear power industry. Several of the task activities are conducted under subcontract by research facilities in the United States and through informal cooperative efforts on an international basis. Currently, four research and development subcontracts are in force.

Administratively, the program is organized into six task areas, as reflected in this report. Sections are included on program administration and procurement, fracture mechanics analyses and investigations, effect of high-temperature primary water on subcritical crack growth of reactor vessel steel, investigations of irradiated material, pressure vessel investigations, and thermal shock investigations.

Eleven program briefings and subcontractor reviews were performed by the HSST staff during this quarter at technical committee meetings, program reviews, and presentations for visitors.

Ten blocks of steel 152 mm (6 in.) by 152 mm (6 in.) square and 304 mm (12 in.) thick from HSST plate 03 were sent to researchers working on a coordinated program on irradiation embrittlement of pressure vessel steels sponsored by the International Atomic Energy Agency. Eight blocks were sent to L. E. Steele of the Naval Research Laboratory and two blocks to W. Spalthoff of the Gesellschaft für Kernenergieverwertung in the Federal Republic of Germany.

All the HSST program materials have been moved for storage in the Y-12 Plant because the storage areas in the Oak Ridge Gaseous Diffusion Plant are no longer available.

A subcontract with the Virginia Polytechnic Institute and State University to perform photoelastic studies of nozzle corner cracks was initiated in January 1976.

2. FRACTURE MECHANICS ANALYSES AND INVESTIGATIONS

2.1 APPROXIMATE STRESS-INTENSITY FACTOR CALCULATIONS FOR CONTINUOUS INSIDE-SURFACE CIRCUMFERENTIAL CRACKS IN A CYLINDER UNDER THERMAL LOADING

J. G. Merkle

2.1.1 Introduction

In the previous progress report,¹ an approximate closed-form solution for the stress-intensity factor for a deep inside-surface continuous longitudinal crack in a cylinder under thermal loading was developed. In this report, the same basic procedure is applied to a continuous inside-surface circumferential crack with an even simpler and equally satisfactory result. In addition, a procedure that avoids division by small numbers is presented for applying the Irwin equation² to relatively shallow continuous surface cracks. Finally, a closed-form expression for the Irwin equation is developed for application to shallow continuous surface cracks subjected to linearly varying nominal stress distributions.

2.1.2 Analysis of Relatively Deep Cracks Considering Shell Bending Effects

Equation for the ligament bending moment

The configuration to be analyzed is defined in Fig. 2.1, which shows the upper half of a cylindrical shell of half length l , thickness w , and mean radius r in which a continuous inside-surface circumferential crack will be introduced in the plane of symmetry after the thermal moments acting on the ends of the uncracked half shell have been removed. After the crack is introduced, the net rotation of the ligament of length b ahead of the crack will be reduced to zero by applying a ligament bending moment M_l , and the value of the crack-tip stress-intensity factor will be computed from the equation^{1,3}

$$K_I = \frac{4M_l}{b^{3/2}} \quad (1)$$

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Fig. 2.1. Definition of the analysis model for a continuous inside-surface circumferential crack in a cylinder.

Note that because there is no axial force under the thermal loading, only one traction and one displacement variable are involved in this analysis; thus, the analysis is simpler than the previously developed one¹ for a thermally loaded longitudinal crack.

Before the introduction of the crack, the rotation θ_1 shown in Fig. 2.1 is related to the moments M_1 and M_2 by the expression³

$$\theta_1 = M_1 \frac{v_1}{\rho D} + M_2 \frac{v_2}{\beta D}, \quad (2)$$

where M_1 and M_2 are moments per radian; v_1 and v_2 are dimensionless influence coefficients, the values of which are tabulated in Ref. 3; and the terms β and D are defined by

$$\beta = \sqrt{3(1 - \nu^2)} \sqrt{\frac{P}{D}} \quad (3)$$

and

$$D = \frac{Eh^3}{12(1 - \nu^2)} \quad (4)$$

The moments per unit of arc length applied to each end of the shell to remove the thermal moments are given by

$$M_1 = -rM_T \quad (5)$$

and

$$M_2 = rM_T \quad (6)$$

Thus, substituting Eqs. (5) and (6) into Eq. (2), the value of θ_1 , termed θ_T , due to the removal of the thermal moments at both ends of the half shell is given by

$$\theta_T = \frac{-rM_T}{\beta D} (v_1 - v_2) \quad (7)$$

The rotation of the crack plane due to shell bending caused by the application of a ligament bending moment M_l per unit of arc length with no change in the moment M_2 is given by

$$\theta = \frac{rM_l v_1}{\beta D} \quad (8)$$

The additional rotation of the ligament region due to the crack that is caused by the moment M_l is given by¹

$$\theta_{lm} = \frac{3M_l}{E\omega^2} \left[\left(\frac{\omega}{h} \right)^2 - 1 \right] \quad (9)$$

For compatibility on the crack plane, which is a plane of symmetry in the analytical model,

$$\theta_T + \theta + \theta_{L\pi} = 0. \quad (10)$$

Substituting Eqs. (7) to (9) into Eq. (10), using Eq. (4), and rearranging gives

$$\frac{M_L}{M_T} = \frac{v_1 - v_2}{v_1 + \frac{2(r/w)[(w/b)^2 - 1]}{\beta^3}}. \quad (11)$$

For $\nu = 0.3$, Eq. (3) reduces to

$$\beta^3 = 2.1238 \left(\frac{r}{w} \right)^2, \quad (12)$$

so that substituting Eq. (12) into Eq. (11) gives

$$\frac{M_L}{M_T} = \frac{v_1 - v_2}{v_1 + \frac{2[(w/b)^2 - 1]}{2.1238 \sqrt{r/w}}}. \quad (13)$$

Equations for the stress-intensity factor

Equation (1), for the stress-intensity factor due to the ligament bending moment, can be rewritten as

$$K_I = \frac{4M_L}{b^2} \sqrt{b}. \quad (14)$$

The thermal moment M_T is related to the linearized surface thermal stress σ_T by

$$M_T = \frac{\sigma_T w^2}{6}, \quad (15)$$

so that substituting Eq. (15) into Eq. (14) gives

$$K_I = \frac{2}{3} \left(\frac{M_L}{M_T} \right) \left(\frac{w}{L} \right)^2 \sigma_T \sqrt{L}. \quad (16)$$

Limiting conditions

For zero crack depth, $b = w$ and Eq. (11) reduces to

$$\frac{M_L}{M_T} = \left(1 - \frac{\nu_2}{\nu_1} \right). \quad (17)$$

Thus, substituting Eq. (17) into Eq. (16) gives

$$K_I = \frac{2}{3} \left(1 - \frac{\nu_2}{\nu_1} \right) \sigma_T \sqrt{w} \quad b = w. \quad (18)$$

This limit is not physically realistic (the correct limit is zero) because Eq. (1) is not accurate for shallow crack depths. However, this inaccuracy is not serious, as noted in Ref. 1, because the Irwin equation can be used for the analysis of shallow surface cracks.

For very deep cracks, Eq. (11) approaches the expression

$$\frac{M_L}{M_T} = 1.0619 (\nu_1 - \nu_2) \left(\frac{b}{w} \right)^2 \sqrt{\frac{r}{w}}. \quad (19)$$

Substituting Eq. (19) into Eq. (16) gives

$$K_I = \frac{2}{3} (1.0619) (\nu_1 - \nu_2) \sqrt{\frac{r}{w}} \sigma_T \sqrt{b}. \quad (20)$$

Thus, as the crack approaches the outer surface, the stress-intensity factor decreases parabolically and tends toward a value of zero at the outer surface. Therefore, complete penetration of the vessel wall by a continuous circumferential crack driven only by an axisymmetric, nonaxially varying, thermal stress distribution seems unlikely as long as the fracture toughness remains finite.

2.1.3 Analysis of Relatively Shallow Surface Cracks Based on the Irwin Equation

Transformation of the Irwin equation for ease of integration

The Irwin equation^{2,4} is an adaptation of the elastic stress-intensity factor solution for a periodic array of coplanar cracks of equal length and identical spacing subject to pairs of opposed crack surface tractions. When applied as an approximation to a shallow edge crack subjected to a nonlinearly varying nominal stress distribution, as indicated in Fig. 2.2, the equation reads^{2,4}

$$K_I = 1.127 \int_0^a \left(\frac{2}{w}\right)^{1/2} \frac{[\tan(\pi a/2w)]^{1/2} \cos(\pi \lambda/2w)}{[\sin^2(\pi a/2w) - \sin^2(\pi \lambda/2w)]^{1/2}} \sigma d\lambda \quad (21)$$

Note that in Fig. A4-1 of Ref. 4, the distances λ and $(a - \lambda)$ were inadvertently reversed. Equation (21) is applicable to continuous surface cracks only if they are relatively shallow, because it is implicitly assumed that the back surface does not develop curvature due to bending.

Fig. 2.2. Definition of terms used in the Irwin equation.

Integration of Eq. (21) is somewhat hampered by the fact that the denominator of the integrand approaches zero as λ approaches a . However, a transformation of the integrand can be used to circumvent this difficulty. Using the substitutions

$$y = \sin \frac{\pi \lambda}{2\omega} \quad (22)$$

and

$$y_a = \sin \frac{\pi a}{2\omega} , \quad (23)$$

Eq. (21) can be rewritten in the form

$$K_I = D \sqrt{\omega} \int_0^{y_a} \frac{\sigma dy}{\sqrt{y_a^2 - y^2}} , \quad (24)$$

where

$$D = \frac{(1.127)(2\sqrt{2})}{\pi} \left(\tan \frac{\pi \sigma}{2\epsilon} \right)^{1/2} . \quad (25)$$

Then, using the substitution

$$\frac{y}{y_a} = \cos \beta , \quad (26)$$

Eq. (24) reduces to

$$K_I = D \sqrt{\omega} \int_0^{\pi/2} \sigma d\beta , \quad (27)$$

where $\beta = 0$ corresponds to the crack tip ($\lambda = a$) and $\beta = \pi/2$ corresponds to the front face ($\lambda = 0$). Defining

$$A = \int_0^{\pi/2} \left(\frac{\sigma}{\sigma_s} \right) d\beta , \quad (28)$$

where σ'_s is the actual nominal stress at the front face, Eq. (27) can be rewritten in the form

$$K_I = \mathcal{D} \sqrt{a} \sigma'_s A . \quad (29)$$

Form of the Irvin equation for very shallow surface cracks subjected to a linearly varying nominal stress distribution

For very shallow cracks, the small angle approximations for the trigonometric functions can be used in Eq. (21), giving

$$K_I = \frac{1.127 \sqrt{\pi a}}{\pi/2} \int_0^a \frac{\sigma d\lambda}{\sqrt{a^2 - \lambda^2}} . \quad (30)$$

If the nominal stress distribution is assumed to vary linearly over the extent of the crack, it can be described by the equation

$$\sigma = \sigma'_s (1 + qu) , \quad (31)$$

where

$$q = \left(\frac{\sigma_a}{\sigma'_s} - 1 \right) , \quad (32)$$

$$u = \frac{\lambda}{a} , \quad (33)$$

and σ_a is the nominal stress at the crack-tip depth. Substituting Eqs. (31) and (33) into Eq. (30) and integrating gives

$$K_I = 1.127 \sigma'_s \sqrt{\pi a} \left(1 + \frac{2q}{\pi} \right) , \quad (34)$$

which is a simple and convenient expression for application to shallow continuous surface cracks.

2.1.4 Analysis Results for the Reference Calculational Model at $t = 7.5$ min

Description of the problem

For evaluation purposes, the foregoing equations were applied to the so-called Reference Calculational Model, which has the characteristics of a typical pressurized-water reactor pressure vessel. The characteristics of the Reference Calculational Model pertinent to this example problem are listed in Table 2.1. Calculated results obtained by the Irwin equation and by the finite-element method, which were already available for this problem⁵ (see Chapter 6), were used as the basis for evaluating the results obtained here.

Table 2.1. Specified conditions for example thermal shock analysis of Reference Calculational Model

Inside radius	2.18 m (86 in.)
Outside radius	2.40 m (95.5 in.)
Cylinder half length	1.40 m (55 in.)
Inside surface heat transfer coefficient	$1140 \text{ W m}^{-2} \text{ }^{\circ}\text{C}^{-1}$ ($200 \text{ Btu hr}^{-1} \text{ ft}^{-2} \text{ }^{\circ}\text{F}^{-1}$)
Initial vessel temperature	288°C (550°F)
Sink temperature	21°C (70°F)
Crack geometry	Continuous, circumferential
Time in transient	7.5 min

The calculated axial stress distribution⁵ in the Reference Calculational Model at $t = 7.5$ min is shown in Fig. 2.3. By fitting the curve given in Fig. 2.3 with a cubic equation and then applying the method of least squares as described in Ref. 1, the linearized surface thermal stress σ_{θ} was found to be 282.5 MPa (40.97 ksi).

Stress-intensity factor calculations for relatively deep cracks

For the Reference Calculational Model, $r = 2.29$ m (90.25 in.), $w = 0.216$ m (8.5 in.), and $l = 1.40$ m (55.0 in.). Values of v_1 and v_2 are

Fig. 2.3. Axial stress distribution in the Reference Computational Model under thermal shock loading at $t = 7.5$ min.

given in Table 5.2 of Ref. 3 as functions of the parameter γ , where

$$\gamma = \frac{\beta l}{r} . \quad (35)$$

Using the value of $\beta = 4.1885$ from Eq. (12) and the values of l and r given above, Eq. (35) gives $\gamma = 2.55$, for which Table 5.2 of Ref. 3 gives $\nu_1 = 1.009$ and $\nu_2 = -0.042$. Substituting these values of ν_1 and ν_2 into Eq. (13) and using $r/w = 10.618$ gives

$$\frac{M_k}{M_T} = \frac{1}{0.9600 + 0.2750 [(w/b)^2 - 1]} , \quad (36)$$

where

$$\left(\frac{a}{b}\right)^2 = \left[\frac{1}{1 - (a/w)^2} \right]^2 \quad (37)$$

For $\sigma_T = 282.5$ MPa (40.97 ksi), Eq. (16) gives

$$K_I = \left(\frac{w}{b}\right)^2 \left(\frac{K_I}{K_T}\right) (188.3) \sqrt{b}, \text{ MN}\cdot\text{m}^{-3/2}, \quad (38)$$

where

$$b = w \left(1 - \frac{a}{w}\right). \quad (39)$$

Using Eq. (18), the limit of K_I (numerically, not physically) as the crack depth becomes zero is found to be $90.9 \text{ MN}\cdot\text{m}^{-3/2}$ ($82.9 \text{ ksi}\sqrt{\text{in.}}$).

For very deep cracks, entering the above determined values of v_1 , v_2 , r , w , and σ_T into Eq. (20) gives

$$K_I = 684.9\sqrt{b}, \text{ MN}\cdot\text{m}^{-3/2}. \quad (40)$$

The calculated values of K_I for a continuous circumferential crack in the Reference Computational Model at $t = 7.5$ min, considering the effects of shell bending, are represented by the triangles shown plotted in Fig. 2.4. The value of K_I for $a/w = 0$ was calculated from Eq. (18), and the values of K_I for $a/w = 0.95$, 0.975 , 0.99 , and 1.0 were calculated from Eq. (40). All the other values of K_I were calculated from Eqs. (36) and (38). It is readily seen that the results calculated here for moderate and large crack depths agree well with those calculated by the finite-element method,⁵ especially for $a/w \geq 0.6$.

Stress-intensity factor calculations by the Irvin equation

The squares shown plotted in Fig. 2.4 represent the results of the calculations made by the Irvin equation. All the K_I values except that for $a/w = 0.5$ were calculated by the linearly varying stress approximation for shallow cracks, Eq. (34). The value of K_I for $a/w = 0.5$ was calculated

Fig. 2.4. Stress-intensity factors for the Reference Calculational Model at $t = 7.5$ min.

from Eqs. (28) and (29) based on the stress distribution given in Fig. 2.3. It is clear from Fig. 2.4 that Eq. (34) is an accurate approximation for $a/w \leq 0.2$ and that the value of K_I for $a/w = 0.5$ is accurate with respect to the curve taken from Ref. 5.

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3. EFFECT OF HIGH-TEMPERATURE PRIMARY REACTOR WATER ON THE FATIGUE-CRACK GROWTH OF REACTOR VESSEL STEEL*

W. H. Bamford   K. V. Scott†

The objective of this task is to obtain fatigue-crack growth rates in terms of fracture mechanics parameters for pressurized-water reactor (PWR) primary coolant environments. During this report period, specimen F-1 was tested in a PWR environment. This specimen was made of A508 forging material and was tested at 1 cpm with an R ratio (K_{min}/K_{max}) equal to 0.7. Results for this test are shown in Fig. 3.1. This is the first specimen of forging material to be tested at low cyclic frequency under this program, and the results show no difference in behavior between the forging and plate data. The only noticeable difference between forging and plate tests thus far is the appearance of the fracture surface. The forging specimen had a smooth and regular fracture surface, with no noticeable crack branching as seen in the plate material. Fractographic examination of the fracture surface, now under way, will provide more information for this comparison.

After removal of specimen F-1, testing was begun on a new specimen (C-1), which is made of weld material from a typical reactor vessel weld on A533B plate material, at 1.0 cpm with an R ratio of 0.7.

Testing continued through the entire report period on specimen 02GB-7 at a frequency of 0.1 cpm. This specimen is part of a series of specimens from HSST plate 02 being tested to characterize the frequency saturation effect for this material. Tests have been conducted at frequencies of 60, 1, 0.5, and 0.1 cpm. Since the tests require long periods to complete, one chamber has been devoted to this task exclusively for the past 18 months. The task is not yet complete, but the results to date, presented in Fig. 3.2, are of interest. The lines shown in the figure represent least-squares best fits of all the data obtained to date at 60, 1, and 0.5 cpm. (A best fit line was not included for 0.1 cpm due to lack of sufficient data.) The data were interpreted by the incremental polynomial method

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*Work sponsored by HSST program under UCCND Subcontract 3290 between Union Carbide Corporation and Westinghouse Electric Corporation.

†Westinghouse Electric Corporation.

Fig. 3.1. Environmental fatigue-crack-growth results for A533B and 508 steel.

Fig. 3.2. Effect of cyclic frequency on crack growth for A533B and A508 steel tested in a PWR environment - preliminary results (tests conducted prior to Feb. 15, 1976). 1 in. = 25.4 mm; 1 $\text{ksi}\sqrt{\text{in.}}$ = 1.0988 $\text{MM}\cdot\text{m}^{-3/2}$.

recommended by ASTM by use of a computer program that was completed during this report period. The method has the advantage of producing more data points per specimen, but it also produces more scatter. The results shown in Fig. 3.2 indicate that there is a considerable difference in crack-growth rates between test results obtained at 60 cpm and those obtained at 1 cpm. Only a slight increase is seen when test frequency decreases to 0.5 cpm. This indicates that the saturation effect is indeed present and is the reason why testing is now being done at 0.1 cpm.

4. INVESTIGATIONS OF IRRADIATED MATERIALS

D. A. Canónico

The influence of irradiation on the mechanical properties of metals has a significant impact on the safe behavior of structural materials employed in nuclear power plants. Of particular concern to the safety of light-water reactors is the loss of toughness that occurs in ferritic materials as a consequence of their exposure to fluence levels in excess of 10^{18} neutrons/cm² ($E > 1$ MeV) at operating temperatures. Investigations are under way at Oak Ridge National Laboratory (ORNL), Westinghouse Electric Corporation (WEC), and Hanford Engineering Development Laboratory (HEDL) to assess the effect of appropriate neutron exposure levels on the ferritic steels employed in the fabrication of primary containment vessels.

The current effort in this area has concentrated on the extension of linear elastic fracture mechanics to irradiated pressure vessel steels. Toward this end, six 101.6-mm-thick [4-in.-thick (4T)]^{*} compact-tension (CT) specimens have been irradiated in the Battelle Research Reactor (BRR) to fluences of about 3.5×10^{19} neutrons/cm² ($E > 1$ MeV) at 288°C (550°F). A number of smaller specimens, Charpy V-notch, precracked Charpy, 0.394T-CT, and tensile, were irradiated by being placed in the pinholes of the 4T-CT specimens. Toughness information obtained with these specimens have served as the basis for the selection of the test temperatures for the 4T-CT specimens.

4.1 FRACTURE MECHANICS EVALUATION OF LARGE IRRADIATED REACTOR VESSEL STEEL SPECIMENS[†]

J. A. Davidson[‡]

The objective of this program is to further justify the applicability of linear-elastic fracture mechanics to irradiated nuclear pressure vessel grade steel. Six 4T-CT fracture mechanics specimens were subjected to

^{*}Note that 1T = 25.4 mm thick.

[†]Research performed under Subcontract No. 3720 for the Oak Ridge National Laboratory operated by Union Carbide Corporation for the Energy Research and Development Administration.

[‡]Westinghouse Electric Corporation.

neutron bombardment at 288°C (550°F) and tested in the postirradiated condition to determine the temperature dependence of the fracture toughness K_{Id} . In addition, 0.394T-CT specimens were included in the program to determine the apparent fracture toughness K_{Id} utilizing the equivalent-energy concept.

Dynamic testing of the two weldment 4T-CT specimens and fluence determinations for all five broken 4T specimens were completed this report period. The test results of the dynamic 4T-CT weld specimen tests are listed below.

Designation	Orientation	Test temperature [(°C) (°F)]	K_{Id} [MN·m ^{-3/2} (ksi √in.)]	$\dot{K}$ [MN·m ^{-3/2} ·sec ⁻¹ (ksi √in./sec)]
W58-2	Weld	37.7 (100)	106.3 (96.7)	0.9×10^b (0.8×10^b)
W58-1	Weld	74 (165)	>237.0 (>215.7)	1.2×10^b (1.1×10^b)

Specimen W58-1 failed to break during testing and exhibited a maximum load of 0.90 MN (202,000 lb). Upon close examination of the precrack tip on the side of the unbroken specimen, it was found that no branching had occurred, as shown in Fig. 4.1. The unbroken specimen was cooled to -18°C (0°F) and broken. Plastic replicas of the fracture at the fracture initiation area were taken and examined using scanning electron microscopy techniques to determine the extent of any possible subcritical crack growth resulting from the 0.90-MN load.

Figure 4.2 shows the typical fracture initiation area at a magnification of 120×. Area A is characteristic of the fatigue precracking, while area C is the cleavage fracture area resulting from breaking the specimen at -18°C. Area B is about 0.13 to 0.23 mm (5 to 9 mils) wide and is at an angle of about 45° to the fatigue and fracture planes. Close examination of area B shows that the fracture surface in this area is characteristic of the fatigue-type fracture appearance of area A rather than the cleavage appearance of area C. Importantly, area B did not resemble a typical ductile fracture morphology and can therefore be considered as part of the fatigue precracking event rather than subcritical crack growth.

Fig. 4.1. Closeup of the fatigue precrack tip area on the side of the unbroken weldment 4T-CT specimen W58-1 tested dynamically at 74°C (165°F).

Figure 4.3 shows a plot of the weld K_{Id} test results as a function of test temperature and previous test results.

Results of drillings taken at each edge and center of the fatigue precrack tip areas of the broken 4T-CT specimens produced the fluences given in Table 4.1. These values are consistent with fluence values reported by J. A. Williams of HEDL for the RW static 4T-CT previously

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Fig. 4.2. Three fracture areas of W58-1 weldment 4T-CT specimen after breaking at -18°C (0°F) [following failure to break during dynamic testing at 74°C (165°F)]. Scanning electron microscopy photographs illustrate fracture areas characteristic of A, fatigue precracking; B, fatigue precracking; and C, cleavage failure.

Table 4.1. Fast-neutron (~ 1.0 MeV) fluence levels derived from the drillings of the five HSST 4T-CT specimens

Sample No.	Orientation	Fluence levels (neutrons/cm ² $\times 10^{-19}$)	
		Fluence	Average
W58-1-1	Weld	3.89	4.43
W58-1-2		3.75	
W58-1-3		5.65	
W58-2-1	Weld	4.06	4.79
W58-2-2		3.93	
W58-2-3		6.19	
W58-3-1	RW	5.30	4.17
W58-3-2		3.35	
W58-3-3		3.86	
OZGA441-1	WR	4.11	3.91
OZGA441-2		3.36	
OZGA441-3		4.30	
OZGA442-1	WR	3.84	4.11
OZGA442-2		3.44	
OZGA442-3		5.05	

Fig. 4.3. Dynamic irradiated K_{IC} properties for HSST weldment W58A compared to unirradiated properties.

tested and are also consistent with fluence values of the Charpy and 1.9T-CT specimens with respect to irradiation location in the Battelle Research Reactor.

4.2 TOUGHNESS INVESTIGATIONS OF IRRADIATED MATERIALS

R. G. Berggren   D. A. Canonico

Preparations for the irradiation of 4T-CT specimens from three submerged-arc weldments are in progress. Three capsules, each containing one weldment material, will be irradiated in the Bulk Shielding Reactor (BSR) at 288°C (550°F) to a target fluence of 1×10^{19} neutrons/cm² ($E > 1$ MeV). Each capsule will contain two 4T-CT specimens, two 1.6T-CT specimens, four 0.8T-CT specimens, thirty-nine 0.5T-CT specimens, sixty-one Charpy-V, eight precracked Charpy-V, and nine miniature tensile specimens. Each capsule will also contain 42 thermocouples to control and monitor temperatures and 37 dosimeter capsules to monitor fast-neutron fluences for the irradiation. All of the 4T, 1.6T, 0.8T, and one-half of the 0.5T-CT specimens have revised notch configurations to facilitate integral analysis of test results.

Fabrication of facility components and instrumentation is nearing completion, assembly of the irradiation capsules has begun, and irradiation is expected to start in May. Preparations are nearing completion for neutron flux and spectrum determinations using "dummy" capsules in the BSR.

4.3 INVESTIGATION OF INSERT TECHNIQUES FOR LARGE FRACTURE TOUGHNESS SPECIMENS

A. J. Moorhead

The objective of this subtask is to develop a method for fabrication of a "composite" 4T-CT specimen for use in evaluating the effect of irradiation on the fracture toughness of ferritic materials. The composite specimen is desirable because it would eliminate the present practice of irradiating entire 4T-CT specimens. Instead, only a central portion of the specimen would be irradiated and subsequently joined to the remainder of the specimen for testing. This method would minimize program costs by

reducing the amount of reactor space required for irradiation tests and allowing more specimens to be irradiated in a given space. Another benefit is that a composite specimen would minimize the amount of material required for a 4T-CT specimen, making it easier and cheaper to test experimental heats or compositions. We are evaluating the feasibility for using four techniques for joining the components of the composite specimen: (1) adhesive bonding, (2) mechanical joining, (3) narrow-gap welding, and (4) out-of-vacuum electron-beam (EB) welding.

During an earlier report period,¹ we determined that the technical feasibility for using the EB welding process to join the components of an irradiated 4T-CT specimen was high; however, since the potential costs for adapting this process for this application were also very high, the process was dropped from active consideration. We do think that EB welding is very feasible for joining composite specimens of materials that are limited in quantity but not irradiated, and we will be making specimens of this type later.

We have temporarily postponed our evaluation of the narrow-gap gas metal-arc welding technique for joining of composite specimens. The company that had agreed to weld the sample composite 4T specimens has notified us that they will be unable to undertake this service in the foreseeable future. We are presently looking for another company with this very specialized welding equipment to make some test welds.

During this report period, we concentrated on further evaluation of the phenolic epoxy adhesive that we had previously looked at in a cursory manner.² A series of samples having either flat or serrated interfaces [0.78 radian $\times$ 7.6 mm (45° $\times$ 0.3 in.) deep grooves] were bonded using American Cyanamid's adhesive HT 424 in the form of either a two-part thixotropic paste or 0.38-mm-thick (15-mil) preimpregnated fiberglass-reinforced tape. The adhesive was cured in air for 40 min at 171°C (340°F) with moderate pressure applied to the bond line by a simple alignment jig. The bonded specimens were tested in tension at room temperature and 177°C (350°F) in an Instron testing machine. Failure occurred after very little yielding, with the results shown in Table 4.2.

These results are encouraging because they indicate that it may be possible to successfully test a 2T or 4T composite CT specimen that has

Table 4.2. Test results of flat- and serrated-interface specimens bonded with phenolic epoxy adhesive^a

Specimen No.	Adhesive form	Interface geometry	Test temperature [°C (°F)]	Load at failure ^b [N (lb)]
1	Paste	Flat	24 (75)	12,900 (2900)
2	Paste	Flat	24 (75)	7,100 (1600)
3	Film	Flat	24 (75)	13,100 (2950)
4	Film	Flat	24 (75)	18,900 (2675)
5	Paste	Flat	177 (350)	8,540 (1920)
6	Paste	Flat	177 (350)	15,100 (3400)
7	Film	Flat	177 (350)	14,100 (3175)
8	Film	Flat	177 (350)	15,100 (3400)
9	Paste	Serrated ^c	24 (75)	28,900 (6500)
10	Paste	Serrated	24 (75)	25,800 (5800)
11	Film	Serrated	24 (75)	18,000 (4050)
12	Film	Serrated	24 (75)	20,000 (4500)
13	Paste	Serrated	177 (350)	22,900 (5150)
14	Paste	Serrated	177 (350)	23,400 (5250)
15	Film	Serrated	177 (350)	28,000 (6300)
16	Film	Serrated	177 (350)	26,100 (5875)

^aAmerican Cyanamid's adhesive HT 424 cured for 40 min at 340°F (171°C).

^b6.4-cm² (1-in.²) specimen cross section.

^c0.78 rad × 7.6 mm (45° × 0.3 in.) deep serrations.

been joined by adhesive bonding. For instance, loads of approximately 0.18 and 0.71 MN (40,000 and 160,000 lb) are required to test 2T- and 4T-CT specimens respectively. Based on these tensile results, the adhesive-bonded serrated joints would be able to carry 0.22 and 0.89 MN (50,000 and 200,000 lb) in a 2T or 4T specimen. The unknowns at this time are how the serrated joints will fare in the complex loading experienced during actual testing and in what manner the composite interface affects the stress distribution at the root of the notch. In order to answer these questions, we are presently having two 2T-CT specimens made from A533 steel. After the crack has been fatigued at the root of the notch,

these specimens will be cut and remachined into two composite specimens with serrated interfaces.

4.4 EVALUATION OF FRACTURE TOUGHNESS OF A533-B USING SMALL SPECIMENS*

J. A. Williams[†]

The measurement of fracture toughness commensurate with the requirements of linear elastic fracture mechanics stress-intensity factor K using small specimens has resurfaced as a recent concern. Small surveillance specimens, primarily of the Charpy V-notch impact type, will necessarily be used for quantitative fracture toughness interpretations in the assurance of safe operation of irradiated nuclear reactor pressure vessels. To date, satisfactory correlations that would relate Charpy V impact data to material toughness can only serve as a behavioral estimate of the effects of irradiation (trends in transition shift and relative upper-shelf energy).

A program was developed to determine the limits and conservatism of small specimens in the assessment of fracture toughness. The near-term objective of the Small Specimen Utilization (SSU) program is to relate the onset of crack extension to maximum load as a function of specimen size so as to determine the utility of small specimens for fracture toughness assessment.

The SSU program exploratory test matrix objectives are shown in Table 4.3. ASTM A533-B steel will be used to assess measurement capabilities of unirradiated materials, and quenched-only A533-B will be used to correlate toughness measurement limits in a material with a higher yield strength to ductility ratio that behaves closely to A533-B in the irradiated condition. Test temperatures are those for which full upper-shelf behavior would be expected. Approximately five specimens of each size will be tested to develop a well defined J curve (J integral vs crack extension) from which

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* Research performed under Purchase Order 11Y-50917V for Oak Ridge National Laboratory, operated by Union Carbide Corporation under contract to the U.S. Energy Research and Development Administration.

[†] Hanford Engineering Development Laboratory.

Table 4.3. Test matrix for small-specimen utilization program

Material	Specimen (number)	Test temperature [°C (°F)]
ASTM A533-B, quenched and tempered, WR	0.394T-CT (5)	121 (250)
	1T-CT (5)	
	2T-CT (5)	
	4T-CT (5)	
	C _v bend (5)	
ASTM A533-B, quenched only, WR	0.394T-CT (5)	177 (350)
	1T-CT (5)	
	2T-CT (4)	
	4T-CT (4)	
	C _v bend (5)	
ASTM A533-B, irradiated ($\approx 2.5 \times 10^{19}$ neutrons/cm ²), WR	0.394T-CT (5)	177 (350)

J_{Ic} may be determined by the intersection method. In the longer term, other temperatures, fluences, and materials (forgings, welds) are considered.

The desirability of a single-specimen J_{Ic} is evident from the number of specimens required to develop the R curve. Current test methods were surveyed by HEDL, and it was determined that a sensitive measure of load drop might signal the onset of crack extension and thereby form the basis of a single-specimen J_{Ic} test. The experimental details of load-drop detection were developed by T. Marston, then of Combustion Engineering. It was then recognized that allowing the test to proceed yields an additional J value for crack extension, thus generating from a single specimen the potential for establishing a J_{Ic} value and an R curve. The concept of such a test is shown in Fig. 4.4. A second major objective of the SSU program was to include the development of the single-specimen J_{Ic} - R curve test; the scope of the SSU test matrix would also act as a confirmation of the single-specimen results.

Progress during the past quarter has been primarily in the development of the load-drop detection techniques. The results of the first of these

SINGLE SPECIMEN J_{Ic} - R-CURVE

Fig. 4.4. Schematic of elastic-plastic fracture toughness test method for determining the onset of crack initiation.

tests, which was conducted at room temperature on A533-B in the RW orientation, are shown in Table 4.4 and Fig. 4.5. In conducting the tests, specimen load is measured as a function of load line crack opening displacement and recorded. Concurrently, the load is recorded from a second high output load cell that is used to detect load changes. The indicated load drop is then coordinated with the load-displacement record to indicate the onset of crack extension and the area under curve A for J_{Ic} analysis. Since there is no established standard testing procedure for J , the procedure proposal of Landes and Begley³ and standards⁴ of fracture testing from ASTM E 399 were incorporated to control the test procedures used. Displacement rates during testing produced a stress intensification of $165 \text{ MN}\cdot\text{m}^{-3/2}$ (150 ksi

Table 4.4. Fracture toughness of 0.394T-CT specimens of ASTM A533B, RW orientation, at room temperature^a

Specimen No.	Crack length (in.)	J_{Ic} (in.-lb/in. ²)	R value	
			J (in.-lb/in. ²)	Δa (in.)
XA17-1	0.404	1054 ^b	2635	0.044
XA19-4	0.401	971	1291	0.013
XA16-3	0.372	1049		
XA20-4	0.412	1044	1899	0.028
XA16-4	0.405	871	1558	0.012
XA20-2	0.400	843	1487	0.016
XA20-1	0.405	840	3373	0.063
XA16-1	0.483	954	1872	0.022
XA19-1	0.504	890	1022	0.007
XA17-4	0.503	892	1390	0.015
XA17-3	0.505	804	2113	0.023
XA16-2	0.494	819		

^a1 in. = 25.4 mm; 1 in.-lb/in.² = 175.13 N·m/m².

^bCalculated from the area under the curve at crack initiation as determined by load drop detection.

$\sqrt{in.}$) during the elastic portion of the test. The values of J were calculated from

$$J = \frac{2A}{bB}, \quad (1)$$

where A is area under the load-deflection curve to the point of interest (initiation point yields J_{Ic} ; total area yields R curve value of J for measured crack extension), b is the initial uncracked ligament length, and B is the specimen thickness. The initiation value, J_{Ic} , from Eq. (1) is plotted on the curve defined by

$$J = 2(\sigma_{flow}) \Delta a, \quad (2)$$

where σ_{flow} is the stress halfway between yield strength and ultimate strength [σ_{flow} was taken to be 531 MPa (77,000 psi) based on Ref. 5].

Fig. 4.5. Elastic-plastic fracture toughness of 0.394T-CT specimen of ASTM A533, grade B, class 1 pressure vessel steel from HSST plate 02, RW orientation, showing J_{Ic} values from crack initiation detected by a load drop method.

The value of J_{Ic} was determined to be 161,000 N·m/m² (920 in.-lb/in.²) from the average of 12 specimens; total scatter was approximately ±13%. It was also observed that the specimens with deeper cracks (smaller uncracked ligament) exhibited a higher resistance to crack extension (steeper R curve). The intersections of the R curves of both the deep crack ($a/w = 0.63$) and standard crack ($a/w = 0.5$) specimens verified the initiation value of J_{Ic} as determined by the load-drop method. Conversion of J_{Ic} to K_{Ic} agrees closely with measured K_{Ic} values from large specimens; the converted value is 192 MN·m^{-3/2} (175 ksi √in.) compared to the measured value of 176 to 198 MN·m^{-3/2} (160 to 180 ksi √in.) by Shabbits et al.⁶

From the results, it appears that the potential exists for a practical single-specimen or reduced-specimen J_{IC} - R curve technique. The development would be significant in fulfilling both surveillance and large-specimen test requirements.

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5. PRESSURE VESSEL INVESTIGATIONS

R. H. Bryan

Tests are being planned for the remaining intermediate test vessels (V-7A, V-8, and V-10). The repaired vessel (V-7A) is being instrumented for retesting under conditions identical to the initial test except that loading will be sustained by pressurization with nitrogen and the utilization of a patch on the interior of vessel opposite the flaw. Plans are being made to repair this vessel again using the Section XI weld repair procedure without stress relief¹ and to test the repaired vessel (V-7B) again with a sharp crack in the repair zone.

Residual stress measurements in the weld procedure qualification piece (the V-9 prolongation) for the V-7 repair¹ are being made. The weld metal from 31.8- to 38.0-mm-thick (1 1/4- to 1 1/2-in.) test coupons has been characterized.

Studies are continuing in preparation for sustained-load testing of vessel V-8, which will be used for crack-propagation and -arrest demonstration tests. Theoretical studies^{2,3} indicate the feasibility of such tests. As with all previous intermediate vessel tests, model vessels will be tested to assure that analytical methods are adequate and to establish the proper test specifications.

5.1 V-7A TEST PREPARATIONS

R. H. Bryan   P. P. Holz

The instrumentation plans for vessel V-7A were adopted from the plan used in the original (hydraulic) test of V-7 so that all the original measurements would be matched with comparable measurements in the sustained-load test. Additional strain gages were located in the weld repair zone, and inside crack-opening-displacement (COD) and crack-propagation gages were planned for the flaw zone. The data-acquisition system has been designed to record the transient response of the vessel if rupture were to occur. This facility was not available in previous intermediate vessel tests.

At the direction of NRC, a patch has been designed so as to prevent or retard the depressurization of the vessel after a through-the-wall crack develops. This assures that the pressure at the time of rupture will indeed be sustained. However, the patch introduced complications in that the inside-surface strain, COD, and ultrasonic instrumentation near the flaw had to be eliminated. Furthermore, a means had to be devised to quickly and surely ascertain the moment at which the flaw completely ruptures the wall.

No substitutes for the eliminated strain and COD measurements could be devised without diminishing the integrity of the patch. K. K. Klindt performed mockup tests of the ultrasonic system for locating the buried crack front and demonstrated that these measurements could be made with transducers located outside the vessel. Consequently, there will be two transducers observing the ligament at the root of the machined notch and one near each end to observe the axial extension of the prepared flaw.

The patch is a thick [406 × 610 × 1.59 mm (16 × 24 × 1/16 in.)] sheet of type 304 stainless steel rolled to conform to the inside surface of the vessel and welded by the tungsten inert-gas process around the edge with INCO 82 filler (see Fig. 5.1). A patch pressurization system was developed and tested in order to sense whether the vessel wall has been ruptured. The general scheme is to maintain the pressure beneath the patch (i.e., between the patch and the inside surface of the vessel) at the same pressure as that throughout the vessel as long as the vessel wall is intact. The pressures in the vessel and in the system communicating with the volume beneath the patch are measured separately with a flow restriction between the points of measurement. When the vessel ligament ruptures, the pressure in the patch system will drop rapidly, provided there is adequate communication between the point of rupture and the patch pressurization system.

Tests were conducted on a patch mockup to establish that such communication would prevail under V-7A test conditions. The small test system shown in simplified form in Fig. 5.2 was set up. In each test a pressure of 138 to 172 MPa (20 to 25 ksi) was first established throughout the system with gas in the system between points A and D. Then, while high pressure was maintained on the outside of the patch mockup (point E), the

Fig. 5.1. Leak-retarding patch for vessel V-7A.

Fig. 5.2. Patch pressurization mockup test system for vessel V-7A.

valve at D was opened to simulate a rupture in the vessel wall. The tests showed that gas flow beneath the patch would be adequate to permit easy detection of a pressure drop between points E and F, provided the passages through the vessel wall are not large enough in diameter and uniform enough at the inside corner surface to allow a seal to develop at the points of entry.

Although smooth finishes were used in the patch mockup to simulate conditions most conducive to sealing, it was not certain that the small mockup patch (see Figs. 5.2 and 5.3) was not, at least in early test cycles, supported in a position slightly separated from the simulated vessel wall. Testing showed that a seal would develop after several cycles, if a passage through the wall was sufficiently large. For these reasons, additional tests were made with a 0.304-mm (0.012-in.) spacer wire between the patch and simulated wall. These tests confirmed consistently good performance of the system. The test system was also used to check flow-restricting elements that would perform satisfactorily and reliably.

The welding procedure for applying the patch to vessel V-7A was evaluated by approximately 1/5-scale patches and by bend specimens as shown in Fig. 5.3. All specimens and patches withstood extreme strains without developing cracks. The weld on the patch in V-7A was inspected with dye penetrant and was leak tested with helium. Weld repairs in some areas were required to attain a leakfree weldment.

5.2 CHARACTERIZATION OF THE WELD METAL IN VESSEL V-7 WELD REPAIR

W. J. Stelzman   D. A. Canonico

Two shielded metal-arc weldments were prepared by Combustion Engineering using the same filler metal and welding procedures employed in the repair welding of intermediate test vessel V-7 and the welding of the V-9 prolongation. All welding was performed in accordance with EPRI* Proposal RFP-4344/01. Each completed weld received a postweld heat treatment of 4 hr at 238 to 274°C (460 to 525°F) followed by a slow furnace cool to room

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* Electric Power Research Institute.

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(b) SIMULATOR FLANGE PATCH DISK INSTALLED (c) BEND TEST SPECIMENS

Fig. 5.3. Patch mockup and bend test pieces for vessel V-7A.

temperature at a cooling rate of 11 to 17 K/hr (20 to 30°F/hr). This post-weld heat treatment is identical to that given the V-7 and V-9 prolongation repair welds. Upon completion of the postweld heat treatment, the weldments were radiographed and found free of rejectable defects. A surface-to-surface cross section of the 32-mm-thick (1 1/4-in.) weldment is shown in Fig. 5.4. The test specimens were taken from the same weldment.

The tensile results from W-oriented specimens of weld metal (specimen length perpendicular to the welding direction and parallel to the weld surface with the fracture plane along the weld vertical center line) over the range of -73 to 177°C (-100 to 350°F) are provided in Table 5.1 and Fig. 5.5. These data indicate minimum yield and ultimate strengths of about 483 MPa at 135°C (70 ksi at 275°F). Two types of tensile specimens were used: the standard 12.8-mm (0.505-in.) gage diameter and the 4.52-mm (0.178-in.) gage diameter miniature tensile specimen. The 12.8-mm specimens were tested at room temperature only to assess the effect of sample area, and no significant differences in tensile properties were noted between the two specimen sizes. The weldment base plate came from the 3/4T depth of HSST plate 02, section 02FH, close to plate section 02FB; therefore, a reasonable comparison can be made with the 02FB tensile data at

Fig. 5.4. Cross section of the shielded metal-arc weld qualification for the weld procedure for intermediate test vessel V-7 repair.

Table 5.1. Tensile values from W-oriented specimens^a from shielded metal-arc weldment W-1

Depth ^b ($d/1.25$)	Test Temperature [°C (°F)]	Strength			Elongation (%)		Reduction in Area (%)
		Upper yield [MPa (ksi)]	Lower yield ^c [MPa (ksi)]	Ultimate [MPa (ksi)]	Uniform	Total	
0.71T	-73.3 (-100)	585 (84.9)	543 (78.8)	655 (95.0)	8	18	72
0.29T	23.3 (74)	543 (78.8)	510 (73.9)	590 (85.6)	5	14	76
0.35T	24.4 (76)		488 (70.8)	586 (85.0)		18 ^d	72
0.35I	24.4 (76)		491 (71.2)	589 (85.4)		18 ^d	72
0.71T	65.6 (150)	507 (73.5)	479 (69.5)	563 (81.6)	6	15	71
0.29T	121 (250)		490 (71.0)	582 (84.4)	2	7	39 ^e
0.71T	177 (350)		490 (71.1)	659 (95.6)	5	10	58
0.29T	232 (450)		497 (72.1)	622 (90.2)	4	10	64

^aUnless noted, data from 0.452-cm (0.178-in.) gage diameter specimens with gage length to diameter ratio = 7 and $\dot{\epsilon} = 0.016 \text{ min}^{-1}$.

^bDepth from top surface of the weld.

^cAbsence of upper yield denotes 0.2% yield.

^dStandard 1.28-cm (0.505-in.) gage diameter specimens with gage length to diameter ratio = 4 and at $\dot{\epsilon} = 0.022 \text{ min}^{-1}$.

^eLow due to lack of fusion and entrapped slag in the fracture zone.

Fig. 5.5. Variation of tensile properties with temperature for shielded metal-arc weldment W-1.

the 1/4 to 1/2T depth levels. Strength results in the 02FB section indicate yield and ultimate strengths of 469 to 483 MPa (68 to 70 ksi) and 614 to 627 MPa (89 to 91 ksi) respectively. Therefore, the weld metal appears to exhibit yield strengths 7 to 41 MPa (1 to 6 ksi) higher and ultimate strengths 21 to 41 MPa (3 to 6 ksi) lower than the base plate. The base plate of the weldment was not characterized.

The toughness results for the weld metal are shown in Figs. 5.6 and 5.7. Charpy V impact results, Fig. 5.6, from WL-oriented specimens (specimen length perpendicular to the welding direction and parallel to the weld surface with the fracture propagating along the weld center line in the welding direction) may also be compared with Charpy V impact data obtained from HSST plate section 02FB. The weld metal exhibits a lower transition temperature [-34°C (-30°F)] than the plate average [-1.1°C (30°F)] at the

Fig. 5.6. Variation of Charpy V impact properties with temperature for shielded metal-arc weldment W-1.

41-J (30 ft-lb) energy level fix and a higher ductile upper-shelf energy [180 J (133 ft-lb)] than the plate average [157 J (116 ft-lb)], indicating superior toughness properties for the weld metal.

The static fracture toughness results for the weld metal using pre-cracked Charpy V bars shown in Fig. 5.7 indicate that the maximum toughness for the Charpy bar [$252 \text{ MPa}\sqrt{\text{m}}$ ($250 \text{ ksi}\sqrt{\text{in.}}$)] occurs between -18 and 38°C (0 and 100°F). Efforts were made to corroborate the "shelf" toughness using IT-CT specimens. Two specimens were provided, but the test results were rejected because of unacceptable precrack fronts (see Fig. 5.8). Both specimens exhibited identical crack fronts. A review of dimensional

Fig. 5.7. Variation of static fracture toughness with temperature for shielded metal-arc weldment W-1.

Fig. 5.8. Fatigued crack front in a 25.4-mm-thick (1-in.) compact-tension specimen precracked at room temperature and tested at 107°C (225°F).

inspection data, fatigue precrack loading, and test grip alignment indicated that all were within specifications and that no reversal of specimen orientation in the grips had occurred during the fatiguing. This would indicate that the strength and/or toughness variation from weld bead to weld bead may have adversely affected the fatigue crack growth. Plans are to examine the tested specimens by light microscopy and hardness measurements to relate the problem to microstructure.

5.3 PRELIMINARY INVESTIGATION OF THE APPLICABILITY OF DYNAMIC FINITE-ELEMENT TECHNIQUES OF THE CRACK-ARREST PROBLEM

G. C. Smith

A simple method of analyzing a propagating crack was discussed in a previous progress report.⁴ Once the crack arrests, it would be desirable to assess the possibility of reinitiation of fracture that could be caused by a dynamic structural response. One potential method of analysis involves dynamic finite-element codes: in this approach, the fast fracture phase of the crack-arrest event could be approximated by a suddenly appearing crack in a loaded structure. This approach was used by Ayres⁵ for the related problem of determining the rate of opening in a ruptured pipe. The output of the proposed analysis would be stresses and displacements as a function of time, which could be compared against a fracture criterion.

Without prior knowledge of the capability of any particular dynamic finite-element program, one would be unsure of the validity of results found by that means. In fact, one would anticipate that the discretization requisite for a finite-element analysis would introduce dispersive effects that could give misleading results. To evaluate that prospect and to determine the potential usefulness of dynamic finite-element techniques in the ORNL intermediate test vessel crack-arrest program, some severe test cases with known exact solutions were run using the NONSAP finite-element program.⁶

The problems considered involved step singularity loading conditions and as a consequence resulted in wave propagation in the structure. The

first problem to be discussed is that of a half-space that is loaded uniformly at the free surface. This was modeled in one case with one-dimensional truss elements and in another with two-dimensional quadrilateral elements. There was essentially no difference in the results from those two models.

For linear elastic, isotropic homogeneous materials, it is known that the exact solution predicts an undispersed wave propagating at the dilatational wave velocity.⁷ Figure 5.9 shows the exact solution as solid lines at several times measured from the instant of loading. The corresponding

Fig. 5.9. Wave propagation in a half space.

dynamic finite-element solution is shown as points, with each point representing the stress at the midpoint of a truss element. The conclusion from this analysis is that although dispersive effects are present, the finite-element method yields a close approximation to the exact solution. The end of the truss model opposite the one that was loaded had a fixed displacement boundary condition. When the wave traveled the full length of the model and encountered the fixed end, the finite-element results indicated a reflection with an amplitude twice that of the applied loading. This is the prediction given by the exact theory. When this wave traveled back to the loaded surface, a rarefaction wave, which again is predicted by the exact theory, was also observed.

The model used to calculate the results shown in Fig. 5.9 incorporated a lumped mass description. The NONSAP program has the consistent mass option, which could give improved results at a greater cost for computing time. The integration scheme used for the results shown in Fig. 5.9 was the Newmark method.⁸ When the Wilson integration scheme⁹ was used, slightly improved results were observed. The principal difference was that away from the predominant dispersive character at the wave front, the Wilson solution was in better agreement with the exact solution.

The second problem considered as a test case, which involved a suddenly appearing finite crack in a two-dimensional infinite elastic solid, was solved exactly by Sih et al.³ The finite-element model consisted of two-dimensional plane strain quadrilaterals. Near the crack tip, the typical quadrilateral dimension was 1/50 the crack length, and away from the crack region, large elements were used to simulate the infinite solid. This is a reasonable representation for times short compared to the transmission time from the crack surface to a remote boundary. The solid line in Fig. 5.10 is the exact solution given by Sih et al., and the points represent the calculations from the displacements given by the finite-element analysis. As in the half-space problem, agreement between the exact theory and the finite-element analysis is quite good. If the mesh of the finite element was made finer, one would expect better agreement. When the integration step size was reduced by a factor of 2, no appreciable difference was noted in the results.

Fig. 5.10. Dynamic stress-intensity factor vs time.

From these sample problems, it would seem that the dynamic finite-element method can produce acceptable results under very severe conditions. Further investigations are planned, and it is likely that dynamic finite-element methods will be useful in the analysis of the ORNL crack-arrest vessels.

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o. THERMAL SHOCK INVESTIGATIONS

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6.1 INTRODUCTION

During this reporting period, activities under the LOCA ECC Thermal Shock Program included the following: a core containing the entire flaw in TSV-2 was trepanned and examined, additional calculations were made in the interpretation of TSE-2, a proposal was made for two additional experiments, stress-intensity factors were calculated for a continuous circumferential flaw in the reference calculational model (RCM) of a pressurized-water reactor pressure vessel, and the thermal shock test facility was modified.

6.2 THERMAL SHOCK EXPERIMENT 1 (TSE-1)

In a previous publication¹ it was reported that, based on COD and acoustic-emission instrumentation, there was no indication of crack extension in TSE-1, but examination of cores trepanned from TSV-1 revealed some extension [3 mm (≤ 0.1 in.)]. It was believed that residual stresses associated with the electron-beam (EB) weld (flaw generation) contributed to the initiation of the existing flaw and that this initiation took place almost immediately upon application of the thermal shock. Additional study of the phenomenon indicates that the extension resulted from subcritical crack growth. Recent experimental data obtained with compact-tension specimens at temperatures corresponding to upper-shelf toughness show that subcritical crack growth takes place at loads equal to about one-half the peak load. Early in the TSE-1 transient, the stress-intensity factor due only to the thermal stress was not sufficient to result in subcritical crack growth. However, in combination with an estimated residual stress of ~ 345 MPa (50,000 psi) at the original crack tip, subcritical crack growth appears to be possible. Supporting evidence for this conclusion is that the fracture surface appears to be mixed mode; that is, the fracture was not brittle.

6.3 THERMAL SHOCK EXPERIMENT 2 (TSE-2)

As discussed in a previous report,¹ data obtained from the COD and acoustic-emission instrumentation indicated that the 19-mm-radius (0.75-in.) semicircular flow in TSV-2 extended along the surface in two events during TSE-2. The upper end extended about 69 mm (2.7 in.) at ~36 sec and the lower end about 43 mm (1.7 in.) at ~86 sec. Following the experiment, the surface cracks associated with these extensions could be seen with the naked eye. During this reporting period, an oblong core containing the entire crack was trepanned from TSV-2 and was cut in several locations to reveal the radial extent of the crack. Typical photomicrographs are shown in Fig. 6.1. Finally, the sections of the core containing portions of the crack were cooled to cryogenic temperatures and fractured to expose the fracture surfaces; examination of the surfaces is in progress. At this time, it appears that the final crack shape is approximately semielliptical, as shown in Fig. 6.2, and that there was essentially no extension at the deepest point of the original crack front.

Assuming that the intermediate and final crack shapes were semielliptical with aspect ratios of 2.7 and 3.7 and using the measured transient temperature distributions and the static fracture toughness curve determined for the test specimen material,² K ratios (K_I/K_{IC}) were calculated for $t = 36$ and 86 sec and for the times at which maximum K ratios occurred. These data are presented in Figs. 6.3 and 6.4 along with the radial temperature distributions at 36 and 86 sec. The calculated K ratio ($b/a = 1$) along the semicircular crack front for $t = 36$ sec is shown in Fig. 6.3. It is observed that over most of the crack front [$(a' > 3 \text{ mm (0.1 in.)})$], the K ratio is less than unity and that for $a' > 8 \text{ mm (0.3 in.)}$ the toughness is on the upper shelf [temperature 121°C (>250°F)]. Even so, the upper end of the crack appears to have extended at this time to the shape shown in Fig. 6.2 (intermediate). This shape can be approximated with a semiellipse having an aspect ratio of ~2.7. The calculated K ratio along the front of this latter crack is shown for $t = 36$ sec in Fig. 6.3. As indicated, there is little difference between the $b/a = 1$ and $b/a = 2.7$ curves. As the transient proceeds, the K ratio all along the crack front increases, achieving maximum values in excess of unity along the entire crack front at times ranging from 3 to 8 min.

Fig. 6.1. Photomicrographs of crack extension in TSV-2 (TSE-2):
 (a) typical new crack at axial location beyond end of original flaw;
 (b) cross section through original flaw showing machined slot, EB-weld crack, and crack extension.

Fig. 6.2. Original semicircular flaw and flaw extension in TSV-2.

Just prior to the 86-sec event, the calculated K ratio along the assumed semielliptical crack front ($b/a \approx 2.7$) was as shown in Fig. 6.4. Here again, only a portion of the crack front [$a \leq 8 \text{ mm (0.3 in.)}$] has a K ratio greater than unity, and for $a > 15 \text{ mm (0.6 in.)}$, the toughness is on the upper shelf. The final length of the crack on the surface was approximately 142 mm (5.6 in.) , which corresponds to an aspect ratio of 3.7. Assuming semielliptical geometry for this aspect ratio, the K ratios at 86 sec are as shown in Fig. 6.4. The figure also shows the maximum K ratios for $b/a = 3.7$ and the approximate times at which they occurred. As before, the increase in aspect ratio (from 2.7 to 3.7 this time) decreased the K ratio some near the surface and increased it slightly elsewhere. The maximum K ratios occur later (3 to 8 min) and are greater than unity along the entire crack front by about 25%.

A possible explanation for the crack behavior described above is as follows. Following initiation of the upper end of the flaw, the crack was able to advance along much of the crack front because the dynamic toughness was sufficiently less than the static toughness to maintain a K ratio of unity. Arrest took place as the bottom portion of the new

Fig. 6.3. K_I/K_{I2} and temperature vs a^* for $h/a = 1$ and 2.7 at $t = 36$ sec and t_{max} : TSE-2.

crack front ran into material operating at upper-shelf temperature, and the stress-intensity factor closer to and at the surface was reduced by the change in crack shape (smaller angle with inner surface). Once again under static conditions, the crack would not move until the static K ratio reached unity at or near the surface. This occurred at 86 sec, with initiation taking place at the lower end because the angle with the surface was steeper than at the upper end. Propagation and arrest took place as

Fig. 6.4. K_I/K_{IC} and temperature vs a' for $b/a = 2.7$ and 3.7 at $t = 86$ sec and t_{max} ; TSE-2.

before. Initiation did not take place again because the K ratio near the surface was less than indicated in Fig. 6.4 due to a shallower angle with the surface than assumed, and at deeper points the crack front was warm prestressed by the time the nominal K ratio passed through unity.

To support the above explanation, reference is made to K_I -vs-time data in Figs. 6.5 to 6.7 for aspect ratios of 1, 2.7, and 3.7 respectively. Also plotted in these figures are the locus of points for $K_I/K_{IC} = 1$.

Fig. 6.5. K_I vs time for TSE-2 showing locus of points for $K_I/K_{Ic} = 1$; semielliptical flaw with aspect ratio $b/a = 1$.

Presumably, warm-prestressing effects exist if a K ratio of unity is achieved after K_I reaches a maximum value. As indicated in Fig. 6.5, warm prestressing did not take place before the first crack propagation event ($t = 36$ sec). At $t = 36$ sec, the geometry changed such that $b/a = 2.7$; Fig. 6.6 shows that prior to $t = 86$ sec (time of the second propagation event), warm-prestressing effects did not exist. At 86 sec, the aspect ratio changed from 2.7 to 3.7, and as shown in Fig. 6.7 points on the crack front deeper than 8 mm (~ 0.3 in.) were warm prestressed.

Fig. 6.6. K_I vs time for TSE-2 showing locus of points for $K_I/K_{I0} = 1$; semielliptical flaw with aspect ratio $b/l = 2.7$.

With regard to the postulate that the dynamic toughness was substantially less than the static toughness, toughness data reported by Shabbits³ and Crosley and Ripling⁴ for A533 quenched and tempered material indicate ratios of static to dynamic toughness in the transition region of about 2 to 3, with the ratios increasing with increasing temperature. Thus, in this regard the above explanation seems reasonable. However, a few dynamic data points obtained at ORNL⁵ for A533 in the quenched-only condition indicate very little difference between static and dynamic toughness in the transition region and a higher dynamic toughness on the upper shelf. If the ORNL data are more appropriate, the above explanation for crack behavior in TSE-2 is not valid.

Fig. 6.7. K_I vs time for TSE-2 showing locus of points for $K_I/K_{IC} = 1$; semielliptical flaw with aspect ratio $b/a = 3.7$.

6.4 PROPOSAL FOR THERMAL SHOCK EXPERIMENTS TSE-3 AND -4

Test results from TSE-1 and -2 tend to validate the methods of analysis for predicting crack propagation. However, because of the wide variation in the K ratios from unity and the fact that two different flaw geometries were used, the accuracy of the calculational procedures cannot be determined very closely. Additional experiments should be conducted in an effort to improve this situation. Two specific experiments making use of

the first test specimen (TSV-1) are proposed at this time. The first (TSE-3) would consist of a more severe thermal shock to TSV-1 in its present condition. Because of the possibility of warm-prestressing or similar effects resulting from TSE-1, a second experiment (TSE-4) is proposed for which a new long axial flaw would be provided in TSV-1 and the severity of the shock would be the same as the preceding one. Assuming the most severe thermal shock attainable in the test facility, the calculated maximum K ratio for these two tests is ~ 1.2 , and thus propagation would be expected.

Results of calculations for TSE-3, assuming no warm prestressing, insignificant residual stresses, and the most severe thermal shock [$T_{w_0} = 288^\circ\text{C}$ (550°F), $T_g = -23^\circ\text{C}$ (-10°F) equivalent to $h = 5.7 \times 10^3 \text{ W/m}^2 \cdot ^\circ\text{C}$ ($10^3 \text{ Btu hr}^{-1} \text{ ft}^{-2} \cdot ^\circ\text{F}^{-1}$)], are presented in Figs. 6.8 through 6.17. Figures 6.8 and 6.9 are the calculated temperature and stress profiles, while Figs. 6.10 through 6.15 are plots of K_I , K_{Ic} and K_I/K_{Ic} vs a/w for different times in the transient. Figure 6.16 is the critical crack depth curve, and Fig. 6.17 is a plot of K_I vs time with a/w a parameter and shows the locus of points for $K_I/K_{Ic} = 1$. Figures 6.10 through 6.15 indicate that a crack depth (a/w) of ~ 0.075 results in the maximum K ratio and that the value of the latter quantity is ~ 1.2 . Figure 6.16 shows that the range of crack depths for which $K_I/K_{Ic} > 1$ is 0.028 to 0.170, and that for $K_{Ia} = K_{Ic}$ the maximum depth to which a crack will propagate is 17% of the wall thickness. Figure 6.17 indicates that for crack depths greater than 10% the stress-intensity factor will be decreasing at the time the K ratio reaches unity. Thus, these cracks will probably be warm prestressed if propagation from a shallower crack takes place in steps ($K_{Ia} > K_{Ic}$).

The existing flaw in TSV-1 has a depth of ~ 0.076 and was subjected to a maximum K_I value of $\sim 88 \text{ MN} \cdot \text{m}^{-3/2}$ ($80 \text{ ksi} \sqrt{\text{in.}}$) during TSE-1. In TSE-3, the K_I value at the time $K_I/K_{Ic} = 1$ will be about $110 \text{ MN} \cdot \text{m}^{-3/2}$ ($100 \text{ ksi} \sqrt{\text{in.}}$). Figure 6.18 shows a comparison of K_I vs crack tip temperature for flaws of depth 0.076 in TSE-1 and TSE-3. It is observed that for any particular temperature during the times of interest, the K_I value for TSE-3 is greater than that for TSE-1. Thus, warm prestressing probably will not be a factor in TSE-3 insofar as previous testing (TSE-1) is concerned.

Fig. 6.8. Calculated temperature distributions for TSE-3.

Fig. 6.9. Calculated thermal stress distributions for TSE-3.

Fig. 6.10. K_I , K_{IC} , and K_I/K_{IC} vs a/w , proposed TSE-3, $t = 2$ min.

Fig. 6.11. K_I , K_{IC} , and K_I/K_{IC} vs a/w , proposed TSE-3, $t = 3.5$ min.

Fig. 6.12. K_I , K_{I0} , and K_I/K_{I0} vs a/w , proposed TSE-3, $t = 5$ min.

Fig. 6.13. K_I , K_{I0} , and K_I/K_{I0} vs a/w , proposed TSE-3, $t = 7.5$ min.

Fig. 6.14. K_I , K_{I0} , and K_I/K_{I0} vs a/w , proposed TSE-3, $t = 10$ min.

Fig. 6.15. K_I , K_{I0} , and K_I/K_{I0} vs a/w , proposed TSE-3, $t = 13.3$ min.

Fig. 6.16. Critical crack depth curve (a/v vs t) for proposed TSE-3.

Fig. 6.17. K_I vs time and a/v , showing locus of points for $K_I/K_{I0} = 1$.

Fig. 6.18. K_I vs temperature at crack tip ($a/c = 0.076$) for TSE-1 and proposed TSE-3.

The possibility of residual stresses must be considered also. In TSE-1, the maximum stress was ~ 620 MPa (90,000 psi), which is well below the yield strength of ~ 830 MPa (120,000 psi). Thus, significant residual stresses should not exist.

For TSE-4, it is tentatively proposed that a second long axial crack with $a/w \approx 0.08$ be machined at a position 180° from the existing flaw. The effect of one flaw on the other has been calculated with a 2-dimensional finite-element analysis, and the results are presented in Fig. 6.19, a plot of K_I for one crack (0°) vs the depth of the other crack (180°) with the depth of the first crack (0°) as a parameter. As shown, the existence of the 180° crack increased K_I for the 0° crack only 1% when the 180°

Fig. 6.19. Stress-Intensity factor for one of two long axial cracks (0°) located 180° apart in TSV-1. Thermal shock same as in TSE-2, lower bound temperature: time in transient = 7.5 min.

crack was 20% through the wall. Since a crack in TSE-3 or TSE-4 is not expected to extend much farther than 20% and since the effect of multiple cracks can be calculated, the proposed two-crack experiment appears appropriate. With regard to residual stresses, the maximum stress in TSE-3 will be 690 MPa (100,000 psi), and thus residual stresses should be negligible.

Following TSE-1, two 25.4-mm-diam (1-in.) cores were trepanned from TSV-1 in order to obtain specimens containing short lengths of the flaw.

The locations of these cores are shown in Fig. 6.20. The cores were trepanned by the electron beam machining (EBM) technique, and thus the holes are not much larger in diameter than the cores 3.3 mm (0.13 in.). These holes will be reamed and mechanically plugged, and the plugs seal welded at the outer surface. It is expected that the existence of the holes and plugs will have no effect on the results of TSE-3 and -4. Following TSE-3, two more cores will be trepanned from the area of the existing flaw and the holes plugged. Cores also will be removed after TSE-4.

Test conditions for proposed experiments TSE-3 and TSE-4 are summarized in Table 6.1.

The above proposal was submitted to NRC in January 1976, and in February 1976 the Thermal Shock Review Committee approved TSE-3 and deferred a decision on TSE-4 until results from TSE-3 become available.

Fig. 6.20. TSV-1 showing flaws for TSE-1, TSE-3, and TSE-4 and locations of cores for TSE-1 and -3.

Table 6.1. Test conditions for TSE-3 and TSE-4

Experiment	TSE-3	TSE-4
Test vessel	TSV-1	TSV-1a
Vessel dimensions, mm (in.)		
OD	533 (21)	533 (21)
ID	24 (9.5)	24 (9.5)
Length	914 (36)	914 (36)
Flow baffle OD, mm (in.)	22.9 (9)	22.9 (9)
Wall temperature (initial), °C (°F)	288 (550)	288 (550)
Sink temperature, °C (°F)	-23 (-10)	-23 (-10)
Coolant	40 wt % alcohol 60 wt % water	40 wt % alcohol 60 wt % water
Heat transfer coefficient, W·m ² ·°C (Btu hr ⁻¹ ft ⁻² °F ⁻¹)	5.7×10^3 (10 ³)	5.7×10^3 (10 ³)
Flaw	Long, axial, $a/w \approx 0.08$ (same as in TSE-1)	Same as in TSE-3 plus same type at 180°
$(K_I/K_{IC})_{\max}$	1.2	1.2

6.5 MODIFICATIONS TO THE THERMAL SHOCK TEST FACILITY (TSTF)

Results from TSE-1, TSE-2, and associated thermal-hydraulic experiments [TSE-F(1) through TSE-F(3)] indicated a substantial degree of circumferential asymmetry in quenching of the test specimen. The asymmetry was of no great concern in connection with TSE-1 and TSE-2 but could be for experiments with deeper flaws. Two factors were identified as the probable cause of the asymmetry: excessive and improper routing of instrument leads in the flow annulus and poor piping design at the inlet to the test section. Fewer instrument leads and better routing will be used in the future, and the inlet piping has been modified to eliminate channeling. Testing to determine the degree of asymmetry with these improvements is under way.

6.5 CALCULATIONS FOR REFERENCE CALCULATIONAL MODEL (RCM)

Two additional crack depths were calculated for the long axial crack in the reference calculational model (RCM) using the two-dimensional finite-element analysis. The fractional depths were 0.85 and 0.90. Figure 6.21 (longitudinal) shows $(K_I)_{\max}$ (maximum with respect to time) vs a/w for the RCM, including the two new points. As may be seen, the curve has a maximum at $a/w \approx 0.85$ with a corresponding $(K_I)_{\max}$ value of $401 \text{ MN}\cdot\text{m}^{-3/2}$ ($365 \text{ ksi}\sqrt{\text{in.}}$). Of course, it should be remembered that the analysis was based on a linear elastic model, which is not appropriate for $a/w > 0.5$. The extent of the error is not known at this time.

Two-dimensional finite-element analysis calculations were made for a continuous circumferential flaw in the RCM assuming the vessel to be infinitely long. The maximum stress-intensity factor (maximum with respect to time) as a function of a/w is shown in Fig. 6.21, where it is compared with $(K_I)_{\max}$ for the long axial crack (finite-element) and with the Irwin solution (no bending effect) for the long axial and continuous circumferential cracks. As indicated, $(K_I)_{\max}$ for the continuous circumferential flaw has a maximum of $148 \text{ MN}\cdot\text{m}^{-3/2}$ ($135 \text{ ksi}\sqrt{\text{in.}}$) at $a/w = 0.5$. It is also observed that for deep cracks, $(K_I)_{\max}$ for the circumferential crack is substantially less than that for the axial crack and that neglecting the bending effect (Irwin solution) results in a rather large error.

Fig. 6.21. $(K_I)_{\max}$ vs a/w for long axial and continuous circumferential flaws in RCM considering (finite element) and neglecting (Erwin) bending effects.

The critical crack depth curve for the continuous circumferential crack is shown in Fig. 6.22. As indicated, the range of fractional crack depth for initiation, assuming no warm prestressing, is 0.01 to 0.5; assuming $K_{I1} = K_{IP}$, the maximum fractional depth to which a flaw might propagate is 0.7.

It is of interest to note that in contrast to the long-axial-crack analysis, the circumferential-crack analysis does not contain the conservatism associated with three-dimensional effects. That is, axial gradients in toughness and the end restraints associated with the heads have essentially no effect on the behavior of the continuous circumferential flaw.

Fig. 6.22. Critical crack depth curve for the reference calculational model, high copper.

6.7 EXAMINATION OF THE CRACKED REGION IN TSV-2

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An oblong, approximately 171 mm (6 3/4 in.) long by 25 mm (1 in.) wide, full-thickness core containing the crack that resulted as a consequence of thermal shock experiment 2 was trepanned from TSV-2 by EDM procedures. Transverse metallographic samples were obtained at a number of locations across the crack. The samples were polished and the crack depths

accurately measured at each location. The locations and the crack depths are indicated in Fig. 6.23, which is a sketch indicating the profile of the crack front in TSV-2. The machined notch is indicated as a semi-ellipse toward the left center of the sketch, and the shaded area beneath the machined notch depicts the extent of the original electron-beam (EB) weld crack. The shock media flow is indicated. The crack front is non-uniform, and the extension, on the average, is about 12.7 mm (1/2 in.) deep. The crack extension at the deepest point of the EB weld is only 1.78 mm (0.07 in.).

We have conducted an extensive metallographic examination of the cracks at the locations indicated by depth measurements in Fig. 6.23. The crack morphology is typified by that shown in Fig. 6.24; the crack

Fig. 6.23. Sketch illustrating the profile of the crack that occurred in TSV-2 as a consequence of the thermal shock experiment. The dimensions are exact measurements of the crack depths at the locations indicated. The semiellipse toward the left of center is the machined notch, and the cross-hatched region below it is the EB weld crack. It is evident that the crack front is not uniform.

Fig. 6.24. Metallographic examination of the crack that occurred in TSV-2 as a consequence of the thermal shock experiment. The crack morphology is typical of that seen in all the crack locations examined metallographically. The crack at this location is 15.0 mm (0.59 in.) deep. The propagating crack appears to seek the manganese sulfide inclusions in the steel. Further, there appears to be a tendency for the crack to tunnel, as suggested by the extremely tight crack at the inner surface of the vessel.

is not sharp but tends to run in a jagged and irregular manner. Indeed, as can be seen in the montage of photomicrographs in Fig. 6.24, there are locations where the crack is extending parallel to the inner surface of the vessel. Further, there is a tendency for the crack to preferentially select the manganese sulfide inclusions as it propagates. No doubt these inclusions provided an easy path for the propagating crack.

The jagged, irregular, bridged morphology of the fracture would indicate that perhaps the failure was not entirely by the frangible mode. Scanning electron microscopy (SEM) examinations of the fracture surface in the area where the crack had extended to a depth of 12.4 mm (0.49 in.) dispels this hypothesis. The SEM examination showed that the fracture surface is cleavage.

6.8 MECHANICAL SIMULATION OF IRRADIATION EFFECT ON THE TOUGHNESS OF PRESSURE VESSEL STEELS

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The results from compact-tension specimens from the TSV-2 prolongation aged for 24 hr at 288°C (550°F) are listed in Table 6.2. The static fracture toughness values tend to be lower for a given test temperature and exhibit less scatter than the precracked Charpy results reported previously.⁶

Table 6.2. Compact-tension results^a from TSV-2 prolongation aged for 24 hr at 288°C (550°F)

Test temperature [°C (°F)]	K_{IC} [MN·m ^{-3/2} (ksi √in.)]	Depth	Specimen size ^b
80.0 (176)	109 (99)	0.96T	0.395T
80.6 (177)	108 (98)	0.76T	0.395T
67.2 (153)	101 (92)	0.91T	1T
79.4 (175)	125 (114)	0.70T	1T
79.4 (175)	111 (101)	0.30T	1T
93.3 (200)	138 (126)	0.50T	1T
65.6 (150)	104 (95)	0.32T	2T
79.4 (175)	110 (100)	0.68T	2T

^a K_{IC} calculated by equivalent-energy method.

^b1T = 25.4 mm thick.

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