Heavy-Section Steel Technology Program

Semiannual Progress Report for April – September 1995

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Abstract

The Heavy-Section Steel Technology (HSST) Program is conducted for the Nuclear Regulatory Commission by Oak Ridge National Laboratory (ORNL). The program focus is on the development and validation of technology for the assessment of fracture-prevention margins in commercial nuclear reactor pressure vessels. The HSST Program is organized in seven tasks: (1) program management, (2) constraint effects analytical development and validation, (3) evaluation of cladding effects, (4) ductile-to-cleavage fracture-mode conversion, (5) fracture analysis methods development and applications, (6) material property data and test methods, and (7) integration of results.

The program tasks have been structured to place emphasis on the resolution of fracture issues with near-term licensing significance. Resources to execute the research tasks are drawn from ORNL with subcontract support from universities and other research laboratories. Close contact is maintained with the sister Heavy-Section Steel Irradiation Program at ORNL and with related research programs both in the United States and abroad. This report provides an overview of principal developments in each of the seven program tasks from April 1995 to September 1995.

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Preface

The Heavy-Section Steel Technology (HSST) Program, which is sponsored by the Nuclear Regulatory Commission, is an engineering research activity 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 in October 1994–March 1995. The work performed by the Oak Ridge National Laboratory (ORNL) and by subcontractors is managed by the Engineering Technology Division (ETD) of ORNL. Major tasks at ORNL are carried out by the ETD and the Metals and Ceramics Division. The following is a list of previous progress reports on this program:

ORNL-4176	NUREG/CR-1627 (ORNL/NUREG/TM-401)
ORNL-4315	NUREG/CR-1806 (ORNL/NUREG/TM-419)
ORNL-4377	NUREG/CR-1941 (ORNL/NUREG/TM-437)
ORNL-4463	NUREG/CR-2141, Vol. 1 (ORNL/TM-7822)
ORNL-4512	NUREG/CR-2141, Vol. 2 (ORNL/TM-7955)
ORNL-4590	NUREG/CR-2141, Vol. 3 (ORNL/TM-8145)
ORNL-4653	NUREG/CR-2141, Vol. 4 (ORNL/TM-8252)
ORNL-4681	NUREG/CR-2751, Vol. 1 (ORNL/TM-8369/V1)
ORNL-4764	NUREG/CR-2751, Vol. 2 (ORNL/TM-8369/V2)
ORNL-4816	NUREG/CR-2751, Vol. 3 (ORNL/TM-8369/V3)
ORNL-4855	NUREG/CR-2751, Vol. 4 (ORNL/TM-8369/V4)
ORNL-4918	NUREG/CR-3334, Vol. 1 (ORNL/TM-8787/V1)
ORNL-4971	NUREG/CR-3334, Vol. 2 (ORNL/TM-8787/V2)
ORNL/TM-4655 (Vol. II)	NUREG/CR-3334, Vol. 3 (ORNL/TM-8787/V3)
ORNL/TM-4729 (Vol. II)	NUREG/CR-3744, Vol. 1 (ORNL/TM-9154/V1)
ORNL/TM-4805 (Vol. II)	NUREG/CR-3744, Vol. 2 (ORNL/TM-9154/V2)
ORNL/TM-4914 (Vol. II)	NUREG/CR-4219, Vol. 1 (ORNL/TM-9593/V1)
ORNL/TM-5021 (Vol. II)	NUREG/CR-4219, Vol. 2 (ORNL/TM-9593/V2)
ORNL/TM-5170	NUREG/CR-4219, Vol. 3, No. 1 (ORNL/TM-9593/V3&N1)
ORNL/NUREG/TM-3	NUREG/CR-4219, Vol. 3, No. 2 (ORNL/TM-9593/V3&N2)
ORNL/NUREG/TM-28	NUREG/CR-4219, Vol. 4, No. 1 (ORNL/TM-9593/V4&N1)
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ORNL/NUREG/TM-147	NUREG/CR-4219, Vol. 6, No. 2 (ORNL/TM-9593/V6&N2)
ORNL/NUREG/TM-166	NUREG/CR-4219, Vol. 7, No. 1 (ORNL/TM-9593/V7&N1)
ORNL/NUREG/TM-194	NUREG/CR-4219, Vol. 7, No. 2 (ORNL/TM-9593/V7&N2)
ORNL/NUREG/TM-209	NUREG/CR-4219, Vol. 8, No. 1 (ORNL/TM-9593/V8&N1)
ORNL/NUREG/TM-239	NUREG/CR-4219, Vol. 8, No. 2 (ORNL/TM-9593/V8&N2)
NUREG/CR-0476 (ORNL/NUREG/TM-275)	NUREG/CR-4219, Vol. 9, No. 1 (ORNL/TM-9593/V9&N1)
NUREG/CR-0656 (ORNL/NUREG/TM-298)	NUREG/CR-4219, Vol. 9, No. 2 (ORNL/TM-9593/V9&N2)
NUREG/CR-0818 (ORNL/NUREG/TM-324)	NUREG/CR-4219, Vol. 10, No. 1 (ORNL/TM-9593/V10&N1)
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NUREG/CR-1305 (ORNL/NUREG/TM-380)	NUREG/CR-4219, Vol. 11, No. 2 (ORNL/TM-9593/V11&N2)
NUREG/CR-1477 (ORNL/NUREG/TM-393)	NUREG/CR-4219, Vol. 12, No. 1 (ORNL/TM-9593/V12&N1)

Executive Summary

W. E. Pennell

The Heavy-Section Steel Technology (HSST) Program is conducted for the Nuclear Regulatory Commission (NRC) by Oak Ridge National Laboratory (ORNL). The program focus is on the development, validation, and application of technology for the assessment of fracture-prevention margins in commercial nuclear reactor pressure vessels (RPVs). The scope of the project includes development and experimental validation of analysis methods, development of testing techniques and the generation of materials property data, integration of analysis methods and materials data into a comprehensive RPV integrity assessment methodology, technology transfer through participation in national and international codes and standards activities, and support to NRC in the area of RPV integrity assessment. Program activities are structured to provide support to NRC for the resolution of specific RPV licensing issues. Licensing issues directly influenced by this technology include the definition of RPV pressure-temperature (P-T) curves and low-temperature overpressure protection relief valve set points, evaluation of RPV integrity under pressurized-thermal-shock (PTS) transient loading, safety evaluations of RPVs containing material with low-upper-shelf (LUS) Charpy energy, and data transfer from small-scale surveillance specimens for application in RPV structural integrity assessments.

1 Program Management

Preparation of the final version of HSST Program 189 for the FY 1995 through FY 1998 performance period (ending December 31, 1998) was completed. The 189 was accepted by NRC, and the research program was implemented.

Research subcontracts were in progress during this reporting period with two universities and two government agencies. The University of Illinois subcontract was for enhancement and verification of computer software to model crack growth using a simplified Gurson model. The University of Maryland performed metallurgical evaluations of fracture surfaces from the HSST Program uniaxial and biaxial test specimens to provide data required for the development and validation of dual-parameter fracture-toughness correlations. The Carderock Division of the Naval Surface Warfare Center (CDNSWC) performed dynamic fracture-toughness tests of A 533 B steel, and the National Institute for Standards and Technology did fracture-toughness testing of full-scale specimens cut from the shell of an RPV from an abandoned nuclear plant. A consulting subcontract was in progress with

Prof. S. T. Rolfe of the University of Kansas. Notification was received that the Department of Energy had approved the destruction of the Pressure Vessel Research Users' Facility (PVRUF) RPV. HSST Program requirements for material from the PVRUF RPV were defined. The remaining test vessel from the series of intermediate test vessels was removed from the K-25 powerhouse building, which is being demolished, and prepared for transportation to the HSST Program materials storage yard at the Y-12 Plant.

The NRC HSST Project Manager and the ORNL HSST Program Manager visited research laboratories in the United Kingdom, Sweden, France, and Germany for an exchange of information on current fracture technology research. Specific sites visited were AEA Technology, Risley, England; the Royal Institute of Technology, Stockholm, Sweden; FRAMATOME, Paris la Defense, France; Electricité de France, Les Renardières Research Center, Moret-sur-Loing, France; and the Franhofer Institute Werkstoffmechanik, Freiburg, Germany. Presentations on licensing issues and ongoing research in the HSST Program were given by the U.S. team, and matching presentations were given by the staffs of the host laboratories. A number of areas of fracture technology research were identified in which future interaction between the HSST Program and the host organizations' programs would be mutually beneficial.

The ORNL HSST Program Manager is an active voting member of the American Society of Mechanical Engineers (ASME) Sect. XI Working Group on Operating Plant Criteria (WGOPC), the Task Group on Application of Nondestructive Evaluation (TGANDE) to Plant Operating Criteria, and the Operability Task Group (OTG). The work of the OTG was completed when a proposed Operability Code Case was forwarded to the WGOPC for review and action. The WGOPC developed a proposed modification to Appendix G of Sect. XI of the ASME Code that implements an improved method for calculating the stress-intensity factor (K_I) at the deepest point of a 6:1 semi-elliptical surface flaw. The improved K_I calculation is based on stress-intensity-factor influence coefficients (SIFICs) developed by the HSST Program. The primary objective for the TGANDE is to provide the WGOPC with a justification for reducing the reference flaw size in Appendix G of Sect. XI of the ASME Code. The justification is to be based on the improved flaw-detection and -sizing capabilities of modern NDE systems.

Executive

The HSST Program semiannual progress report for the period September 1993 to March 1994 was completed and transmitted to the NRC HSST Project Manager for review and publication. The ORNL HSST Program Manager presented papers on HSST Program research at the ASME Pressure Vessel and Piping Division Conference in Honolulu, Hawaii. A presentation outlining the basic logic behind the HSST Program research related to development and refinement of PTS analysis methods was given at the Electric Power Research Institute Pressurized Thermal Shock Workshop in Scottsdale, Arizona. During the current reporting period, HSST Program personnel published 3 letter reports, 1 foreign trip report, 1 paper in a refereed journal, and 7 papers in professional society publications and gave 12 presentations at program reviews and technical society- and NRC-sponsored national and international meetings.

2 Constraint Effects Analytical Development and Validation

The focus of this task is on the development and validation of dual-parameter correlations to reflect the effect of crack-tip constraint on the fracture toughness of pressure vessel steels. Crack-tip constraint conditions of particular concern in reactor pressure vessel structural integrity evaluations are those associated with shallow flaws and biaxial loading.

Fabrication of cruciform specimens for the biaxial loading constraint-effects tests continued. Material for the cruciform specimens was heat treated during the prior performance period. A cut-up plane was prepared to provide a total of 40 blanks from which intermediate-scale cruciform specimens can be machined. Thirty-two cruciform specimens are being fabricated. Twenty-four of these are for the load effects validation matrix, and 8 are for the flaw depth validation matrix. The plate material was cut into cubes and then cut again along the original plate midplane. The specimen blanks were machined such that the final "top" surface corresponded with the midplane of the original plate. This approach assures similar material properties through the crack depth for each of the specimens. At the close of this report period, 19 test blocks had been machined from the blanks, welding of extension arms to the test blocks had been done to produce 10 uniaxial assemblies, fatigue pre-cracking of 8 uniaxial assemblies had been completed, and 4 complete cruciform specimens had been assembled.

Characterization tests of the heat treated Plate 14 material were performed. Charpy, drop-weight, and tensile specimens were tested to determine T_{CV} , T_{NDT} , RT_{NDT} , and $\bar{A}_y$ for the material. The 67.8-J (50-ft-lb) energy level was determined to be at a temperature (T_{CV}) of 56°C (132°F).

A comparison of the Charpy data for Plate 14 with Charpy data for a high copper weld, irradiated by the Heavy Section Steel Irradiation Program to a total fluence of 1.51×10^{19} neutrons/cm² ($E > 1$ MeV), showed the two data sets to be similar. The heat treated Plate 14 material, therefore, has impact energy absorption properties that resemble those of an irradiated RPV material. Ten drop-weight samples were also tested, and the drop-weight temperature (T_{NDT}) was determined to be 40°C (104°F). Using these data, the reference temperature for nil-ductility transition (RT_{NDT}) for the heat treated Plate 14 material was determined to be 40°C (104°F). A series of tensile specimens was fabricated and tested. The material yield stress at NDT [40°C (104°F)] was determined to be 620 MPa (90 ksi), which is in the range of yield stress values for irradiated RPV material.

Five additional tests on cruciform specimens with constant-depth shallow flaws were completed in this report period. Test specimens for three of these tests were existing archive specimens from the development stage of the testing program. These specimens were tested to gain experience with the biaxial fracture characteristics of high-yield-stress material. The test specimens were fabricated from plate material with a room temperature yield stress of 380 MPa (55 ksi), which was subsequently heat treated to increase its yield stress to 538 MPa (78 ksi). NDT for the heat treated material was estimated to be near 41°C (105°F). The tests were conducted at room temperature, giving a T-NDT value of -17°C. One test was run with 1:1 biaxial loading, and two were run with uniaxial (1:0) loading. Fracture toughness results from these tests were 95.6 (87), 137.7 (125.3), and 84.2 (76.6) MPa $\sqrt{m}$ (ksi $\sqrt{in.}$) for the biaxial and two uniaxial specimens, respectively. Biaxial loading did not produce the lowest fracture toughness in these tests. These results suggest that the normalized test temperature may be in the lower shelf range for high-yield stress material.

The first test of a specimen fabricated from the heat treated HSST Plate 14 material was conducted under uniaxial loading at the nil-ductility temperature [40°C (104°F)] for this material. The specimen failed by plastic collapse and did not fracture. To better understand the reason for this unanticipated result, a series of 32 1/2T compact tension specimens was machined from the corner drop-offs from the cruciform specimens. The CT specimens were tested at a range of temperatures, and a mean trend curve was fitted to the resulting fracture toughness data. The mean curve was then adjusted for size effects to a 4T specimen size. The resulting fracture toughness curve indicated that valid cleavage initiation tests for the heat treated Plate 14 material should be performed at temperatures much less than 40°C (104°F). The second specimen was, therefore, tested at -30°C (-22°F). The second test was also run under

uniaxial loading. Failure occurred by cleavage fracture, and the test specimen response was essentially elastic up to the point of fracture. The uniaxial fracture toughness obtained from this test was $103.0 \text{ MPa}\sqrt{\text{m}}$ ($93.7 \text{ ksi}\sqrt{\text{in.}}$). Five additional tests are planned at this temperature in the next performance period.

Examination of the fracture surfaces for selected cruciform specimens is in progress at the University of Maryland under an HSST Program subcontract. In the current report period the locations for the main cleavage initiation sites were determined, and measurements of the ductile tearing zone were made. In the next report period the fracture surfaces will be sectioned near the fracture initiation sites, and estimates of the size of the local plastic zone will be made. Information from this research will be used to validate dual-parameter constraint-effects fracture toughness correlations.

3 Evaluation of Cladding Effects

Stainless-steel cladding can act to inhibit the initiation and propagation of cracks from finite-length surface flaws. This property is not reflected in existing RPV structural integrity analysis methods. Objectives for this task are to (a) experimentally determine the influence of cladding on crack initiation from finite-length surface flaws in a prototypical RPV biaxial stress field, and (b) develop and validate the technology required for incorporation of those effects into RPV structural-integrity-assessment methods.

Work continued on shallow-flaw fracture toughness testing of full-thickness clad beam specimens fabricated from a shell segment from the RPV from a canceled pressurized-water reactor (PWR) plant. In a prior phase of this program, tests were conducted on specimens in which the tip of the shallow flaw was located in the longitudinal structural weld of the RPV shell segment. In the current phase of the testing program, three additional full-thickness clad beam specimens were fabricated and tested. The tips of the shallow cracks in these specimens were located in prototypical full-thickness plate material in the region of the clad heat affected zone (HAZ). The thickness and depth for the test-beam cross section were 299 mm and 226 mm, respectively. One of the flaws had a flaw depth/thickness ratio (a/w) of 0.1 ($a = 22.3 \text{ mm}$), while the other two had an a/w ratio of 0.05 ($a = 10.06 \text{ mm}$ and 10.17 mm). In each case the initial flaw was introduced by electrodischarge machining and sharpened by fatigue precracking prior to testing to failure.

Testing of the full-thickness clad beams was performed at the National Institute for Standards and Technology using

the 53.4-MN (12-Mlb) servohydraulic test machine.

Ductile tearing occurred in all three of the test specimens prior to failure by cleavage fracture. Examination of the fracture surfaces revealed ductile tearing extension of the crack, prior to cleavage initiation, of 5.7 mm and 2.5 to 2.7 mm for the $a/w = 0.1$ and the $a/w = 0.05$ specimens, respectively. Impact and tensile data were generated for the weld, clad, and plate material for use in the test data evaluation. Analysis of the results from these tests will be performed in the next reporting period.

In the clad-cruciform-specimen testing program, fabrication of the specimens continued. Detailed cut-up plans and fabrication packages were prepared for reactor pressure vessel shell segments #1 and #2. Blanks for 28 clad-beam specimens were fabricated, together with material for small materials characterization specimens. An upgrade to the 550-kip servohydraulic test machine at ORNL was completed. The upgrade increased the capacity of the machine to approximately 680 kip. Design of a new test fixture was completed. The new test fixture, together with the increased test machine capacity, will permit testing of beams up to 6 in. in cross section and 60 in. long. These developments provide the capability for ORNL to investigate different flaw depths and sizes and to test at temperatures above NDT, where ductile tearing effects are of concern.

Investigation continued into the effects of the stainless steel cladding overlay on cleavage fracture initiation from shallow surface flaws in an RPV under PTS loading conditions. Previously completed deterministic analyses defined the envelopes of minimum flaw sizes required to achieve cleavage initiation for specified PTS transients. In the current report period, this study was extended to determine the effects of clad modeling on probabilistic predictions of cleavage fracture initiation in an RPV under PTS loading. Clad yield stress and fracture toughness properties were represented in the analytical models. Probabilistic fracture initiation assessments were carried out with the FAVOR fracture analysis program. Reductions of approximately one order of magnitude in the predicted probabilities of crack initiation from shallow semicircular surface flaws were produced by the combined effect of clad fracture toughness and yield stress properties. Data from the previously described clad-cruciform tests will provide the means to refine and validate these predictions.

4 Ductile-to-Cleavage Fracture-Mode Conversion

This task investigates the effect of prior ductile tearing on cleavage-fracture-initiation toughness. Ductile tearing prior

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to cleavage fracture initiation is frequently observed in tests performed at temperatures corresponding with the lower-transition region of the fracture-toughness curve. Evidence exists that ductile tearing may act to increase crack-tip constraint and thereby influence fracture toughness. During this report period, work continued on the characterization of precleavage tearing behavior and development of a fracture-mode-conversion model.

The process of precleavage ductile tearing and mode conversion to cleavage was investigated metallurgically using existing test results. Objectives for the investigation were to (1) identify and characterize the microstructural features associated with microvoid formation and growth and (2) identify and characterize the microstructural features responsible for the mode conversion to cleavage. Material selected for the initial investigation was the weld metal from the RPV from a canceled nuclear plant. Inclusions present in the weld metal were characterized using specimens that were metallographically polished and examined in a scanning electron microscope. The size distribution and spacing of the inclusions were determined and compared with corresponding data from fractographic examination of an actual fracture surface. Results from this comparison showed the mean particle size (MPS) from the fractographic examination (0.87 mm) to be larger than the MPS obtained from the metallographic examination (0.50 mm). Results from this investigation provide a means for adjusting particle size and distribution data from metallographic examinations and providing data suitable for use in void growth and coalescence models.

Development of a ductile-tearing model continued during this reporting period, with emphasis on determination of the material parameters required for the Gurson-Tvergaard (G-T) dilatant plasticity model for void growth and an element extinction model for crack growth. The G-T model is incorporated in the WARP3D finite-element computer program. The WARP3D program was used in posttest analyses of the full-thickness clad beam test CB-1.5, which experienced precleavage ductile tearing. Material characterization parameters required for the G-T model are the initial void volume fraction, f_0 ; the void fraction for ductile-tearing failure of a cell, f_c ; and the characteristic length, D , associated with the G-T crack-plane elements. A parametric study was performed using the experimental load vs crack-mouth-opening displacement (CMOD) data from CB-1.5

to determine values of f_0 , f_c , and D that would permit the WARP3D program to duplicate the observed test CMOD and crack-growth behavior. Values of f_0 used in the parameter study were in the range 0.006 to 0.01, the critical void volume fraction f_c was 0.15, and the length scale D was set at 200 μm . Using these parameters, the applied load had to

be increased by 12% in the analytical model to achieve a match between the model prediction and the test load-CMOD curve. The need for this load adjustment is thought to result from the plane-strain model used in the analysis overpredicting the stiffness of the finite-width test beam.

Development and verification of the WARP3D finite-element code continued at the University of Illinois under the direction of Prof. R. H. Dodds, Jr. Emphasis in the current reporting period was on the numerical modeling of ductile fracture in three-dimensions (3-D). Computational cells and node-release models were investigated for modeling 3-D ductile crack growth. The computational cell model predicts the growth and coalescence of voids in material ahead of the crack tip using a G-T constitutive model for dilatant plasticity. Processors for crack growth eliminate the element from the model when voids within an element grow to a critical size. The node-release model grows the crack by removing constraints applied to nodes on the crack plane when local deformation levels reach a critical value based on the opening angle at the crack tip. In parallel developments, (1) modifications were made to the Dodds-Anderson toughness-scaling model to render the model applicable to biaxial loading conditions, and (2) the Weibull stress approach for predicting size effects on cleavage fracture was extended to include the effects of ductile crack extension.

5 Fracture Analysis Methods Development and Application

Development of the Fracture Analysis of Vessels, Oak Ridge (FAVOR) computer program continued. FAVOR is an advanced program for the analysis of RPV failure rates under PTS transient loading. The program is being developed in a manner that will make an executable version of FAVOR available to users without making the source deck available to them. This feature makes possible configuration control of the program.

Work continued on the generation of stress intensity factor influence coefficients (SIFICs) for finite-length semielliptical inner-surface flaws in RPVs. Modifications were made to the finite-element model to more accurately represent the elliptical flaw geometry. Refined SIFICs for RPVs with an internal radius-to-wall thickness ratio (R_i/t) of 10 were published and incorporated into the FAVOR RPV fracture analysis computer program. SIFICs for axially oriented flaws in an RPV with an R_i/t ratio of 20 were generated. These R_i/t ratios encompass a large percentage of the commercial pressurized-water reactor and boiling-water reactor RPVs in the United States.

Support of the ongoing NRC evaluation of the existing PTS regulations continued. PTS screening criteria (RT_{PTS}) are defined in Title 10, Part 50, Sect. 61, of the Code of Federal Regulations (10 CFR 50.61). The basis for the screening criteria is given in the "NRC Staff Evaluation of Pressurized Thermal Shock" (SECY-82-465). The focus of the work in this task in the current reporting period was an evaluation of the potential impact of current fracture models and materials data on the PTS screening criteria. Parameters investigated included flaw density, beltline model configuration, clad plasticity, and shallow-flaw fracture toughness with biaxial-loading-effects adjustments. Updating the SECY model to reflect current modeling practices produced a decrease of 58°F in RT_{PTS}, with the major impact resulting from the inclusion of beltline plate material in the model. Introduction into the model of results from recent RPV-materials research produced an opposite impact on RT_{PTS}, with the net effect of all the parameters investigated (exclusive of flaw density) being an increase of 7°F in RT_{PTS}. Introduction into the analysis model of shallow-flaw fracture toughness data, with biaxial-effects correction, was responsible for most of the positive movement in RT_{PTS}. The overall conclusion from this study was that the values of RT_{PTS} currently defined in 10 CFR 50.61 were compatible with current PTS analysis models and materials data. Results from this study were presented at the NRC/Industry Workshop on Reactor Pressure Vessel Integrity in July 1995.

6 Material Property Data and Test Methods

This task provides support to the other fracture technology development tasks and performs evaluations of specific RPV materials issues. In this reporting period a draft of a NUREG report entitled "Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials, Data Analysis" was completed. The report covers the testing of seven heats of modified A 302 grade B steel and one early vintage heat of A 533 grade B steel. J-R curves were generated for a range of test temperatures, crack-plane orientations, and specimen sizes. Over 230 compact specimens were tested. It was determined that the J-R curve toughness of modified A 302 grade B steel of typical production practice does not degrade with increased specimen size. The results from an earlier Materials Engineering Associates experiment, which showed a significant loss in J-R curve toughness with increased specimen size, were not confirmed in this series of tests.

The Carderock Division of the Naval Surface Warfare Center (CDNSWC) at Annapolis, Maryland, is performing dynamic fracture-initiation testing of an RPV steel under an HSST Program subcontract. A section of HSST plate 14 was shipped to CDNSWC for use in fabricating test

specimens, and preliminary characterization testing of the material was completed. A readiness review was conducted at the CDNSWC facilities, and the review team recommended that CDNSWC be released to perform the tests subject to the prior completion of specific recommended actions. It is anticipated that the dynamic fracture-toughness testing program will be completed by the end of February 1996.

7 Integration of Results into a State-of-the-Art Methodology

The primary objective for this task is to extract and interpret fracture technology advances generated in the HSST Program research tasks, the Heavy-Section Steel Irradiation Program, and other fracture technology research programs, and assemble them into a validated state-of-the-art methodology for assessing the structural integrity of irradiation-embrittled RPVs containing flaws. In addition, this task is responsible for the rapid transfer of fracture technology advances to national consensus codes and standards. Task 7 also provides special technical assistance to NRC for the evaluation and resolution of RPV structural integrity issues.

During the current reporting period the influence of prior ductile tearing on estimates of K_{IC} from small-specimen fracture-toughness tests has been investigated. Two phenomena that have been proposed to explain the observed elevation of fracture-toughness data obtained from small-specimen tests are (1) loss of crack-tip constraint with a consequent reduction in the magnitude of crack-tip stresses and (2) statistical size effects related to the length of crack front exposed to elevated crack-tip stresses. A review of previously published data from small specimen tests showed that the test specimen size effect is reduced, and may be eliminated, when ductile tearing precedes cleavage fracture. Application of the both the Irwin β_{IC} deterministic size-effect correction and the Weibull statistical size-effect correction, neither of which considers tearing effects, showed that size-effect corrections can be overestimated when tearing occurs in the small-specimen tests prior to cleavage fracture. The conclusion from the review was that the neglect of ductile tearing effects would lead to an overestimate of size-effect adjustments of small-specimen fracture-toughness data for test temperatures higher than RT_{NDT}. An empirical adaptation of the Irwin β_{IC} size-effect correction was developed to reflect the effect of ductile tearing on crack-tip constraint and small-specimen fracture toughness in the temperature range $0^\circ\text{C} \leq (T - RT_{NDT}) \leq 50^\circ\text{C}$. The adaptation reduces Irwin's empirical correlation factor linearly over the transition range from a value of 1.4 at $T - RT_{NDT} \leq 0^\circ\text{C}$ to zero at $T - RT_{NDT} \geq 50^\circ\text{C}$. Application of the modified Irwin β_{IC} size-effect correction eliminated the overcorrection problem noted previously.

Heavy-Section Steel Technology Program Semiannual Progress Report for April–September 1995

1 Program Management*

W. E. Pennell

1.1 Objectives

The Heavy-Section Steel Technology (HSST) Program is conducted for the Nuclear Regulatory Commission (NRC) by Oak Ridge National Laboratory (ORNL). The program focus is on development and validation of technology for the assessment of fracture-prevention margins in commercial nuclear reactor pressure vessels (RPVs).

RPV structural integrity issues of current concern to the NRC are defined in the FY 1995 Statement of Work (SOW) for the HSST Program. Six technical issues are identified in the SOW: (1) crack-tip constraint effects on fracture toughness of RPV materials, (2) effects of stainless-steel cladding on the initiation and propagation of shallow surface cracks, (3) ductile-to-cleavage fracture-mode conversion, (4) fracture analysis methods development, (5) materials property data and test methods development, and (6) integration of results from the research programs into a state-of-the-art methodology for RPV structural integrity assessments. The HSST Program is structured to provide the research results required for resolution of these issues.

1.2 Organization and Staffing

Management direction and control of the program are implemented using a seven-element Level 1 work breakdown structure (WBS) and a linked cost-schedule performance-monitoring system. The current HSST Program Level 1 WBS is shown in Fig. 1.1. Each element of the Level 1 WBS represents a separate research or management task with a designated task leader. Within each of the Level 1 WBS elements, a Level 2 WBS defines research subtasks, and a Level 3 WBS defines the individual milestones within a subtask.

Staffing for the research tasks is drawn from the Engineering Technology, Metals and Ceramics, and Computing

*This report is written in metric units. Conversions from English units for all SI quantities are listed at the end of the report.

Figure 1.1 Level 1 work breakdown structure for the HSST Program

Applications divisions at ORNL. Subcontracts with consultants, universities, and other research laboratories are used to gain access to special expertise and capabilities required for certain research tasks. A summary of resources applied to the HSST research tasks during this report period is given in Fig. 1.2.

1.3 Planning

Preparation of the final version of HSST Program 189 for the FY 1995 through FY 1998 performance period (ending December 31, 1998) was completed. The final 189 included negotiated adjustments to the Task 5 milestone schedule and a directed increase in the scope of the test matrix for the cruciform tests, plus an addition to the scope of the "NRC Assistance" subtask. The 189 was accepted by NRC, and the research program defined in the 189 was implemented.

Figure 1.2 Resources applied to the HSST Program R&D tasks

1.4 Subcontracts and Materials Procurement

Research subcontracts were in progress during this reporting period with the University of Illinois (UI), the University of Maryland (UM), the Carderock Division of the Naval Surface Warfare Center (CDNSWC), and the National Institute for Standards and Technology (NIST). A consulting subcontract was in progress with Professor S. T. Rolfe of the University of Kansas. A report from the previously completed readiness review of the dynamic fracture-toughness program at CDNSWC was reviewed and approved, and the program was released for implementation. Discussions were held with Professor Rolfe to define the scope of required consulting support for the HSST Program for the next 12-month period, and a procurement package for this consulting subcontract was prepared and issued. Requests were received from UI and CDNSWC for no-cost extensions (NCEs) of the period of performance for their respective HSST Program subcontracts. The UI subcontract is for enhancement and verification of computer software to model crack growth using a simplified Gurson model. The NCE action was necessary because of a delay in placing this subcontract. The interagency agreement (IAG) with CDNSWC is for dynamic fracture-toughness testing of A 533 B steel. The NCE request for this IAG was made because problems with the 500-kip high-rate testing machine at CDNSWC had delayed the test program. An evaluation of the impact of these NCEs on the balance of the HSST Program activities was made, and it was determined that the schedule delays could be accommodated. Contractual action was taken to implement the NCEs.

Notification was received that the Department of Energy had approved the destruction of the Pressure Vessel Research Users' Facility (PVRUF) RPV. Plans for cutting the PVRUF RPV are being handled by the Heavy-Section Steel Irradiation (HSSI) Program. HSST Program requirements for material from the PVRUF material were defined. The HSST Program will obtain the equivalent of one 7-ft-wide by 12-ft-long panel from the beltline region of the vessel. The panel will be cut such that a longitudinal vessel weld runs down the center of the 7-ft dimension. This material will be available for future test specimen fabrication.

The remaining test vessel from the series of intermediate test vessels was located in the the pressurized-thermal-shock (PTS) test facility in the abandoned powerhouse at the K-25 Site. Demolition of the K-25 powerhouse building is in progress. The PTS-10 vessel was removed from the building debris and placed in an adjacent location from where it will be transported to the HSST Program materials storage yard at the Y-12 Plant.

1.5 International Interactions

The NRC HSST Project Manager and the ORNL HSST Program Manager visited research laboratories in the United Kingdom, Sweden, France, and Germany for an exchange of information on current fracture technology research. Specific sites visited were AEA Technology (AEAT), Risley, England; the Royal Institute of Technology (RIT), Stockholm, Sweden; FRAMATOME, Paris la

Defense, France; Electricité de France (EDF), Les Renardières Research Center, Moret-sur-Loing, France; and the Franhofer Institute Werkstofftechnik (IWM), Freiburg, Germany. Presentations on licensing issues and ongoing research in the HSST Program were given by the U.S. team, and matching presentations were given by the staffs of the host laboratories. Information of special interest to the HSST Program, that was obtained from these interactions, follows.

United Kingdom. Surveillance data from Nuclear Electric's Magnox reactors have shown that the design allowance for the effects of irradiation and thermal embrittlement of the RPV materials will be exceeded before the end of the intended operating life of these plants. A comprehensive evaluation of the structural integrity of the Magnox RPVs is in progress. Biaxial wide-plate tests in the AEAT biaxial testing facility at Risley are being evaluated as a means of demonstrating enhanced resistance to fracture of the RPV material under prototypical loading conditions. The interim conclusion from the AEAT investigation of the proposed wide-plate tests is that the maximum demonstrable benefit in terms of fracture-toughness margin enhancement is achievable only if data from the proposed wide-plate tests can be augmented with data from less expensive tests. Data from the HSST biaxial test program were used in the AEAT design review of the wide-plate test, and the AEAT research team has concluded that essential supplementary biaxial fracture-toughness data for the Magnox RPV material could be generated most efficiently by the HSST Program biaxial test team at ORNL.

Sweden. RIT is investigating crack-tip constraint effects with a matrix of uniaxial fracture tests in which the test temperature, crack depth, and specimen width are all test variables. Micro-mechanical modeling is used to develop an understanding of the crack-tip material behavior for tests conducted in the transition-temperature range. A unique feature of the RIT program is the use miniature tensile specimens to determine the stress-strain properties of material in the crack-tip plastic zone of fracture-toughness specimens that have been subjected to subcritical loading. The tensile specimens have a 0.25- by 0.25-mm cross section and gauge lengths in the range 5 to 10 mm.

France. EDF personnel presented a model developed by EDF and AEAT to predict tearing-cleavage mode-conversion (TCMC) in the transition range of the fracture-toughness curve. The method is based on a coupling of the Rousselier model for ductile tearing with the Weibull model for cleavage fracture. Both of these models are derived from the "local approach" to modeling the fracture process. Ductile-tearing data (J-R curves) and cleavage-

fracture data (K_{Ic} or K_{Jc}) derived from tests on 249 single-edge-notch-bend (SENB) specimens of A 533 B material constitute the database for development of the TCMC model. Elastic-plastic finite-element analysis of the SENB specimens was performed to define the Weibull stress (σ_W) and the J integral corresponding with each of the failure loads for the series of 17 of the SENB tests in which failure occurred by cleavage fracture with no significant prior ductile tearing at a test temperature of approximately -70°C . The Weibull modulus "m," which characterizes the scatter of data in the Weibull weakest-link model, and the fracture stress (σ_U) of the material were determined from these analyses and test results. In the local approach σ_U and "m" are assumed to be material constants that do not vary with temperature. Tests that exhibited ductile crack extension prior to cleavage fracture were analyzed using a constitutive relationship that includes the material-softening effects of void formation and coalescence (the Rousselier damage model). The value of σ_1 , which is the matrix resistance to ductile tearing, was determined from these analyses. The material constants determined from the analysis of the SENB specimens were included in a TCMC model. Inclusion of the Rousselier damage model in the material constitutive relationship ensures that the effects of prior crack growth and local damage to the crack-tip material are included in the calculation of σ_W at failure for test specimens that experienced significant ductile crack extension prior to cleavage fracture. This is the essential feature of the EDF-AEAT coupled approach to TCMC and fracture prediction in the transition-temperature range. The tearing-cleavage model has been used to estimate the lower bound to a K_{Jc} data set generated by AEAT. The predictions matched the lower bound of the data set in the temperature range $-75^\circ\text{C} \leq T \leq -20^\circ\text{C}$, but failed to match the upswing in the lower bound of the data above that temperature. The investigators are evaluating possible temperature dependence of the macroscopic cleavage stress as means of improving the transition-range performance of the TCMC model.

Germany. IWM has conducted research aimed at development and validation of technology for the synthesizing of J-R curves using local-approach-based tearing analysis methods and data from small surveillance specimens. An essential element of the development was adaptation of the established local-approach ductile-fracture analysis methodology such that the required material property input data could be derived from material in the existing RPV surveillance program. The required material properties are the critical void volume fraction f_c for incipient microvoid coalescence and the characteristic length l_c over which the calculated void volume fraction must be averaged for comparison with f_c . IWM has developed a practical means of estimating the constraint-dependent J-R curves using data from relatively small uniform-diameter tensile specimens.

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These tensile specimens can be small enough to be fabricated from material cut from the broken halves of the Charpy specimens or small segments of material taken from the near-surface region of either clad plates or an RPV. The method is based on a modified form of the Gurson flow function, which is incorporated into the material constitutive relationships. The material constant f_c is obtained from the small uniform-diameter tensile specimens using an extension of Bridgeman's analysis of stresses and strains in the neck of a tensile specimen after the limit of uniform extension has been exceeded. The constitutive relationships used by IWM include the effects of material softening by both void growth and adiabatic heating in the form of a modified Gurson flow function. IWM research has shown that the void volume fraction corresponding with the onset of void coalescence (f_c) is not influenced by straining rates. A constant value of f_c was, therefore, used for both the J-R curve synthesis analysis and the analyses of side-grooved precracked Charpy specimens, which were tested at high strain rates at a range of input energy levels. Synthesis of a J-R curve that matched the J-R curve obtained from the precracked Charpy test data was the means used to validate the methodology. The German electric utility company Preussen Elektra AG has successfully used J-R curves synthesized using the IWM technology, and material constants from irradiated surveillance specimens, to demonstrate that adequate safety margins existed for each of five RPVs fabricated from material with low-upper-shelf (LUS) Charpy energy.

1.6 Codes and Standards

The ORNL HSST Program Manager (PM) is an active voting member of the American Society of Mechanical Engineers (ASME) Section XI Working Group on Operating Plant Criteria (WGOPC), the Task Group on Application of Nondestructive Evaluation (TGANDE) to Plant Operating Criteria, and the Operability Task Group (OTG). The work of the OTG was completed when a proposed Operability Code Case was forwarded to the WGOPC for review and action.

The WGOPC developed a proposed modification to Appendix G of Sect. XI of the ASME Code that implements an improved method for calculating the stress-intensity factor (K_I) at the deepest point of a 6:1 semielliptical surface flaw. The improved K_I calculation is based on stress-intensity-factor influence coefficients (SIFICs) developed by the HSST Program and published in letter report ORNL/NRC/LTR-93/33. The HSST SIFICs permit the effects of clad/base-material differential expansion to be included in the K_I calculation, but this feature was not included in the proposed Appendix G modification. The

WGOPC plans to determine the potential effects of cladding on RPV integrity evaluations. As an initial step in that evaluation, the HSST PM (1) collected HSST Program reports and papers and forwarded them to the chairman of the WGOPC and (2) gave a presentation to the working group entitled "An Interim Report on Cladding-Effects Research." The presentation covered both previously completed and ongoing HSST Program cladding-effects research.

The primary objective for the TGANDE is to provide the WGOPC with a justification for reducing the reference flaw size in Appendix G of Sect. XI of the ASME Code. The justification is to be based on the improved flaw-detection and -sizing capabilities of modern NDE systems. Results from an HSST Program staff assessment of flaw size effects under Appendix G loading conditions were presented to the TGANDE. These results showed that the reduction in the applied K_I resulting from the reference flaw size was smaller than may have been anticipated. A reduction in the reference flaw size could still be very beneficial, however, in that it would support the case for reconsideration of the fracture-toughness curve to be used in Appendix G pressure-temperature (P-T) curve calculations.

1.7 Reports, Papers, and Presentations

The HSST semiannual progress report for the period September 1993 to March 1994 was completed and transmitted to the NRC HSST Project Manager for review and publication. Abstracts were prepared for papers entitled "Effects of Shallow Flaws and Biaxial Loading, on the Fracture Behavior of Reactor Pressure Vessels" and "Evaluating the Safety of Aging Nuclear Reactor Pressure Vessels." The papers are scheduled for presentation in 1996 at the Fourth International Conference on Nuclear Engineering (ICONE-4) and at the American Power Conference, respectively.

The ORNL HSST PM gave a number of presentations during this reporting period. Presentations on shallow-flaw, biaxial-loading, and cladding-effects research were given at each of the fracture technology research centers in England, Sweden, France, and Germany. Two papers on HSST Program research entitled "Assessment of the Fracture Behavior of Weld Material from a Full-Thickness Clad RPV Shell Segment" and "An Interim Report on Shallow-Flaw Fracture Technology Development" were presented at the ASME Pressure Vessel and Piping Division Conference in Honolulu, Hawaii. The papers were published in the conference proceedings. A presentation outlining the basic logic behind the HSST Program research related to development and refinement of PTS analysis methods was given

at the Electric Power Research Institute (EPRI) Pressurized Thermal Shock Workshop in Scottsdale, Arizona. A paper by W. E. Pennell and W. R. Corwin entitled "Reactor Pressure Vessel Structural Integrity Research" was published in *Nuclear Engineering and Design*.

During the current reporting period, HSST Program personnel published 3 letter reports,¹⁻³ 1 foreign trip report,⁴ 1 paper in a refereed journal,⁵ and 7 papers in professional society publications⁶⁻¹² and gave 12 presentations at program reviews and technical society- and NRC-sponsored national and international meetings.¹³⁻²⁴

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- * Available in NRC PDR for inspection and copying for a fee.
† Available in public technical libraries.

2 Constraint Effects Analytical Development and Validation

W. J. McAfee

The purpose of this task is to investigate and quantify the effects of constraint on the cleavage-initiation toughness of RPV steels in the lower-transition-temperature region. The work is composed of complementary analytical and experimental efforts. Task 2 is divided into two subtasks: 2.1 Biaxial Loading Effects Validation Testing, (i) Verification Phase Testing and (ii) Analytical Support, and 2.2 Constraint Effects Correlation. Progress during this reporting period for each subtask is discussed below.

2.1 Cruciform Specimen Fabrication

(W. J. McAfee and W. F. Jackson)

The source material for cruciform specimens, HSST plate 14, for the biaxial-loading-constraint verification tests was successfully heat-treated and accepted for use during the last performance period. A cut-up plane was prepared to provide a total of 40 blocks, $241 \times 241 \times 116$ mm ($9 \frac{1}{4} \times 9 \frac{1}{4} \times 4 \frac{1}{2}$ in.) for use in the testing program. Twenty-four of these blocks are to be machined into specimens for the load-effects-validation matrix and eight for the flaw-depth-validation matrix. The remaining blocks are to be archived as spares or used as source material for characterization material. A schematic of the cutting layout is shown in Fig. 2.1. Bars were first cut across the width (the 1219-mm direction) of the plate. These bars were next cut into cubes and then cut again along the original plate mid-plane. Each cube then yielded two specimen blanks with the dimensions noted above. Prior to any cutting, identification markings were applied to the "top" and "bottom" surfaces of the plate to retain both the location and orientation of each block as the plate was cut. The specimen blocks identified as "A" came from one side of the plate; all "B" blocks came from the opposite side. Thus, blocks P2A and P2B are mirror images of each other through the plate thickness with the final "top" surface of the specimens being the mid-plane of the plate. This system ensures that the properties of each specimen through the crack depth will be essentially the same. In selecting the blocks for use as specimens, a random selection scheme was used to ensure that any material variability due to location in the plate would not bias the final test results. Note in Fig. 2.1 that an uncut segment of the plate remains with nominal dimensions $1219 \times 1570 \times 232$ mm ($62 \times 48 \times 9 \frac{1}{8}$ in.). This material is to be used for the fabrication of large-scale (nominally 150-mm-thick) cruciform specimens to be fabricated and tested in FY 1997.

Detailed design was completed for the intermediate-size specimen components and fabrication was initiated. The specimen design is essentially the same as that used in the development phase with the exception that the thickness is 102 mm (4 in.) rather than 91 mm (3.6 in.). The fabrication sequence is also the same as used previously: (1) the test section is fully machined, (2) longitudinal beam arms are electron beam (EB) welded to the section, (3) the EB-weld buildup is removed, (4) the specimen is fatigue precracked, (5) the transverse beam arms are EB-welded to the test section, and (7) final machining is performed to remove the EB-weld buildup and to blunt each end of the flaw. This last operation is to lower the stress concentration at the intersection of the test flaw and the center, transverse load diffusion control slot (LDCS). The status of specimen fabrication at the end of this performance period is summarized below:

• Test blocks machined	19
• Uniaxial assemblies completed	10
• Fatigue precracking completed	8
• Cruciform assemblies completed	4

2.2 Material Characterization

(W. J. McAfee)

Preliminary characterization of the heat-treated plate 14 material has begun, and most of the work has been during this period. Block P8A was used for source material for Charpy, drop-weight, and tensile specimens. The results are described below.

2.2.1 Charpy and Drop-Weight Tests

(W. J. McAfee, D. E. McCabe, and W. F. Jackson)

Charpy V-notch (CVN) tests and drop-weight tests (DWTs) were performed to determine T_{CV} and T_{NDT} for the heat-treated plate 14 material. Twelve Charpy specimens were machined in the T-L orientation. The test results are shown in Fig. 2.2. The 67.8-J (50 ft-lb) energy level was determined to be at 56°C (132°F) using the average curve. The lateral expansion data from these tests are shown in Fig. 2.3. Not enough tests were run to establish

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Figure 2.1 Cutting plan for heat-treated segment of HSST plate 14

Figure 2.2 Charpy results for heat-treated plate 14 and comparison with HSSI weld 73W in unirradiated and irradiated condition, showing successful heat treatment in yielding impact properties representative of irradiated material

Figure 2.3 Expansion results measured from Charpy tests of heat-treated plate 14 material

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T_{CV} precisely using expansion values, but use of a mean curve shows that the 0.889-mm (0.035-in.) limit occurs between 55°C and 60°C, which is consistent with the 67.8-J energy limit. For the intended use, T_{CV} was taken as being 56°C (132°F). Since the objective of the heat treating was to provide a material that had mechanical properties similar to an irradiated pressure vessel steel, a comparison was made between the Charpy results for plate 14 and one of the high-copper welds irradiated by the HSSI Program. Figure 2.2 includes this comparison as trend lines for the HSSI Fifth Irradiation Series Weld 73W in the unirradiated and irradiated [1.51×10^{19} neutrons/cm² (>1 MeV)] conditions¹ compared to the Charpy data for heat-treated plate 14. The trend of the Charpy data for plate 14 almost matches that of the irradiated 73W. It can then be concluded that, respecting behavior as measured by impact properties, plate 14 very much resembles an irradiated material (Table 2.1).

Ten standard P-2 type drop-weight samples were also tested starting at a temperature of -5°C (23°F) and extending up to 45°C (113°F). From these tests, the drop-weight temperature was determined to be 40°C (104°F). Since

$$T_{CV} = 56^\circ\text{C} < 78.3^\circ\text{C} = (45^\circ\text{C} + 33.3^\circ\text{C}) \\ = (T_{NDT} + 33.3^\circ\text{C}), \quad (2.1)$$

Table 2.1 Summary of Charpy results for heat-treated HSST plate 14

Specimen No.	Temperature [°C (°F)]	Energy [J (ft-lb)]	Expansion [mm (in.)]
CH1401	22 (72)	33.1 (24.4)	0.356 (0.014)
CH1402	100 (212)	88.8 (65.5)	1.372 (0.054)
CH1403	50 (122)	55.6 (41.0)	0.864 (0.034)
CH1404	0 (32)	38.9 (28.7)	0.483 (0.019)
CH1405	-50 (-58)	17.9 (13.2)	0.178 (0.007)
CH1406	150 (302)	89.2 (65.8)	1.346 (0.053)
CH1407	200 (392)	90.0 (66.4)	1.473 (0.058)
CH1408	75 (167)	93.3 (68.8)	1.321 (0.052)

drop-weight controlled reference temperature and $RT_{NDT} = 40^\circ\text{C}$ (104°F). The T_{NDT} was used to establish the tensile tests temperatures and as a starting point for the cruciform test temperate.

2.2.2 Tensile Tests (W. J. McAfee)

A series of standard 6.35-mm (0.25-in.) gage diameter tensile specimens were machined from material near the mid-plane of the plate and at four locations through the half-thickness. The tensile characterization results are summarized in Table 2.2. Tests were performed at NDT

Table 2.2 Summary of tensile properties for HSST heat-treated plate 14

Specimen No.	Specimen center ^a (mm)	Test temperature (°C)	Yield stress (MPa)	Ultimate strength (MPa)	Elastic modulus (GPa)	Uniform elongation (%)	Total elongation (%)	Reduction in area (%)
TH01	22.2	40	632.1	794.3	209.6	5.8	13.7	50.8
TH02	38.1	40	619.8	778.4	190.0	6.3	17.1	60.0
TH03	54.0	40	605.6	768.4	185.4	6.1	18.4	63.5
TH04	69.9	40	607.2	761.8	227.6	5.5	14.8	62.6
TH05	22.2	40	632.1	806.0	227.1	6.2	16.3	53.8
TH06	38.1	40	614.2	769.4	194.7	5.7	16.9	58.1
TH07	54.0	40	605.9	761.3	194.2	6.1	16.7	58.1
TH08	69.9	40	606.3	760.9	225.8	5.8	16.7	61.2
Average			615.4	775.1	206.8	5.9	16.3	58.5
TH09	22.2	60	637.2	802.8	211.1	5.1	15.4	57.3
TH10	22.2	60	634.5	801.6	204.8	5.0	13.1	31.6
Average			637.2	802.8	211.1	5.1	15.4	44.5
TH11	22.2	10	652.8	828.4	197.3	6.8	13.2	50.2
TH12	22.2	10	645.1	817.2	216.2	6.5	14.2	32.0
Average			652.8	828.4	197.3	6.7	13.7	41.1
TH13	22.2	-30	668.5	847.9	224.7	6.5	18.6	50.0

^aMeasured from original midplane of plate.

[NDT = 40°C (104°F)], NDT - 70°C, NDT + 20°C, and NDT - 30°C. These temperatures were selected as being the initially expected test temperature range for the cruciform specimens. Four locations through the thickness of the plate were also sampled. The variation of tensile properties with both temperature and location through the plate thickness is relatively small, as is shown in Figs. 2.4 and

2.5, respectively. For example, yield strength decreases ~5% between -30°C to 60°C. At a test temperature of 40°C, yield strength decreases ~4% from near the midplane of the original plate to 57 mm (2.25 in.) from the midplane. A typical stress-strain curve is shown in Fig. 2.6. Reference stress-strain curves for analysis use have been constructed from the tensile data at each of the test temperatures.

Figure 2.4 Tensile properties of heat-treated plate 14 over temperature range -30°C (-22°F) to 60°C (140°F)

Figure 2.5 Tensile properties of heat-treated plate 14 relatively independent of location through plate thickness [test temperature = NDT = 40°C (104°F)]

Figure 2.6 Typical stress-strain curve for heat-treated plate 14 at 40 °C (104°F) showing yield near 620 MPa (90 ksi) with sharp departure from linear behavior, low-strain hardening, and reduced ductility

2.2.3 Compact Tension Tests (W. J. McAfee, D. E. McCabe, and W. F. Jackson)

As will be discussed in Sect. 2.4, the first cruciform specimen was tested at 40°C (104°F). The specimen failed by plastic collapse rather than by cleavage. It was concluded that, before testing additional cruciform specimens, more information was needed about the shape of the toughness curve through the lower-transition-temperature region. A series of 32 1/2T compact tension (CT) specimens was machined using material from corner drop-offs from the cruciform specimens. The cruciform specimens are themselves machined from a block ~234 × 234 × 102 mm (9 1/4 × 9 1/4 × 4 in.) by a continuous-pass wire electro-discharge machine (EDM) process. The scrap material “drops off” the specimen in the form of rectangular blocks 66 mm (2 5/8 in.) square and 102 mm (4 in.) thick. Blocks were taken from four different specimens and used as stock material for machining of 32 1/2T specimens with the flaw oriented in the T-S direction. The first set of eight specimens, all taken from a single drop-off, were tested at -30°C (-22°F). The results are shown in Fig. 2.7. An exponential “mean” trend curve was fitted to these data to provide some idea of the variation of K_{Jc} with temperature. The “mean” curve was then adjusted for size effects to a 4T-size specimen. From this curve, it was apparent that valid cleavage-initiation tests for the heat-treated plate 14 material need to be performed at temperatures much less than 40°C (104°F). A logical choice would be the temperature at which the 1/2T CT specimens were tested, that is, -30°C, since all the 1/2T specimens provided valid cleavage data and underwent plastic deformation prior to failure. Additional 1/2T tests are currently being performed at tem-

peratures over the range -130°C (-202°F) to 23°C (73°F). These tests will provide information for assessment of the cruciform tests results.

2.3 Testing of Special Cruciform Specimens (W. J. McAfee, J. W. Bryson, and W. F. Jackson)

As was reported in the last semiannual progress report, extensive work was performed on developing a heat-treatment procedure for achieving simulated irradiated material properties. In performing the heat-treatment development work, heat treating of the Combustion Engineering (CE) material was also considered. A procedure that was developed and applied to the CE material was to normalize for 4 h at 1010°C (1850°F) followed by a rapid air quench. This procedure was shown to elevate the room-temperature 0.2% offset yield strength of the CE plate material from 380 MPa (55 ksi) to near 538 MPa (78 ksi) although the stress-strain response was highly nonlinear, the proportional limit being near 241 MPa (35 ksi). As an exercise, this heat treatment was applied to an archive beam specimen, HTBB06. This specimen was initially fabricated from the CE plate material and was to be part of the loading-ratio-verification matrix. The specimen was damaged during removal of the first set of EB-weld run-off tabs and was replaced in the matrix. During the heat-treatment development work, it had been proposed to heat-treat and test this specimen. The beam was heat-treated and fabrication was completed, but it was decided not to test the beam because a different heat-treatment procedure was selected

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Figure 2.7 Fracture-toughness data from 1/2T CT specimens of heat-treated plate 14; "mean" trend curve for 1/2T CT data and "mean" trend curve adjusted to 4T-size specimen also shown

for use with plate 14 (i.e., for specimens to be tested in the verification matrix). HTBB06 was then shelved for potential future use. It was concluded that valuable information about the fracture behavior of an elevated-yield-strength material could be obtained from this specimen while awaiting fabrication of specimens from heat-treated plate 14. This plan was discussed with the NRC staff, and approval was received from to test specimen HTBB06. Approval was also obtained to heat-treat and complete fabrication of two additional specimens remaining from the loading-ratioverification matrix. Fabrication and testing of all three specimens was completed.

Using data previously developed for the heat-treated CE material, it was estimated that NDT would be near 41°C (105°F). Room-temperature testing at 24°C (75°F) would then be both convenient and in the range of $T - NDT$ used for the biaxial development tests ($T - NDT \approx 10^\circ\text{C}$). HTBB06 was then instrumented and tested to failure under 1:1 biaxial loading.

The failure results for these three tests are summarized in Table 2.3. The crack-mouth-opening displacement

Table 2.3 Summary of failure results for cruciform specimens fabricated from CE material heat-treated to an elevated yield strength of 538 MPa (78 ksi)

Parameter	HTBB06	BBHT07	BBHT08
Biaxial ratio	1:1	0:1	0:1
Test temperature, °C (°F)	23 (74)	23 (74)	23 (73)
Longitudinal failure load, kN (kips)	618 (138.9)	730 (164.2)	476 (107.0)
CMOD, mm (in.)	0.201 (0.0079)	0.368 (0.0145)	0.163 (0.0064)
LLD, mm (in.)	2.570 (0.1012)	4.569 (0.1799)	2.357 (0.0928)
η_{pl}^c	5.051	0.615	1.025
η_{pl}^l	0.656	4.990	7.011
P vs LLD, $\text{MPa}\sqrt{\text{m}}$ ($\text{ksi}\sqrt{\text{in.}}$)	96.6 (87.9)	137.0 (124.7)	84.2 (76.6)
P vs CMOD, $\text{MPa}\sqrt{\text{m}}$ ($\text{ksi}\sqrt{\text{in.}}$)	95.6 (87.0)	137.7 (125.3)	87.9 (80.0)
NDT, ^a °C (°F)	-5 (23)	-10 < NDT < +10 (14 < NDT < 50)	20 (68)
$T - NDT$, ^a °C (°F)	28 (51)	23 (42)	3 (5)

^a NDT determined only approximately using four drop-weight samples machined from each specimen.

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(CMOD) test records for each of these three specimens (HTBB06, HTBB07, and HTBB08) are shown in Figs. 2.8, 2.9, and 2.10, respectively. HTBB06 (1:1) was predominantly an elastic test as is shown by the load-CMOD curves of Fig. 2.8. The nomenclature used in Fig. 2.8 for identifying CMOD measurements is s (south), n (north),

m (middle), and fs (far south) clip gages. The results for HTBB07, Fig. 2.9, indicate more ductility and, ultimately, a higher toughness than that observed for HTBB06. This is consistent with the development-phase tests where the 1:1 biaxial load case had the potential to yield the lowest deformation and toughness of the cases studied. The maximum

Figure 2.8 CMOD results for test of heat-treated CE material, cruciform specimen HTBB06; load ratio = 1:1, test temperature = 23°C (74°F)

Figure 2.9 CMOD results for test of heat-treated CE material, cruciform specimen HTBB07; load ratio = 0:1, test temperature = 23°C (74°F)

Figure 2.10 CMOD results for test of heat-treated CE material, cruciform specimen HTBB08; load ratio = 0:1, test temperature = 23°C (73°F)

CMOD values for HTBB06 were ~75% of those measured for specimen BB10, which prior to this test represented the lowest toughness result obtained from the cruciform specimens. For comparison, the differences in deformation behavior for this specimen and selected cruciform specimens tested in the development and validation effort is illustrated by centerline CMOD data for HTBB06 (1:1), HTBB07 (0:1), BB10 (1:1), BB05 (0.6:1), and BB11 (0:1) as shown in Fig. 2.11. The increased stiffness of HTBB06 and HTBB07 due to increased yield is apparent by comparison of the load-CMOD traces.

The behavior of HTBB08 did not agree as well with the other results as was expected. While some degree of data scatter is always associated with experimental data, the results from HTBB08 seemed to be inconsistent compared to those of HTBB06 and HTBB07. Comparison of the fracture surfaces for these three specimens showed that the one for HTBB08 had a smoother texture, the type associated with a material tested on the lower shelf. By carefully removing the fracture surfaces as a thin [~6-mm (1/4-in.)] slice, sufficient material remained to make four drop-weight specimens from each test section. The drop-weight specimens were prepared and tested, and the results are included in Table 2.3. With so few samples, only an

approximation could be made for NDT. It appears, however, that NDT for specimen HTBB08 was well above those for the other two specimens, which would have led to the test temperature being closer to the lower shelf. The cause for the variation in NDT for these three specimens is not clear although variations in the cooling rate during heat-treatment could be responsible. It is clear, however, that HTBB06 and HTBB07 form one population while HTBB08 is in another and that the results cannot be compared directly.

Analyses were performed for these three specimens using the stress-strain curve developed for the heat-treated CE material. The plastic zone widths at failure were determined for each test and are plotted in Fig. 2.12 in comparison with the verification matrix data. Specimens HTBB06 (1:1 loading) and HTBB07 (0:1 loading) follow the same trend behavior as was displayed for the as-received CE material (verification matrix specimens). Note that the as-received CE material and the heat-treated CE material were tested at different normalized temperatures so that comparison only of trends is appropriate. The trends continue to verify the strain-based dual-parameter fracture-toughness model that has been proposed.²

Figure 2.11 Comparison of CMOD results for uniaxial and biaxial cruciform tests, as-received and heat-treated material, illustrating differences in deformation behavior

Figure 2.12 Comparison of K_J - $\ln(R)$ results for as-received and heat-treated CE plate material

2.4 Testing of Plate 14 Cruciform

Specimens (W. J. McAfee, J. W. Bryson, and W. F. Jackson)

Procedures for fatigue precracking of the heat-treated plate 14 specimens followed those developed previously for the development and verification matrix specimens. This material exhibited a high resistance to crack initiation (fatigue) and crack growth, requiring as much as an order of magnitude more cycles to achieve the desired change in compliance as compared to previous tests. The first specimen, P2A, was used to verify the fatigue precracking procedures. Since the crack growth characteristics of the material were unknown, a low level of ΔK was used initially. The specimen was cycled between bending-load limits of 22 and 200 kN (5 and 45 kip) to impose a ΔK of 24 MPa $\sqrt{m}$ (22 ksi $\sqrt{in.}$). After ~30,000 cycles, no measurable change in compliance had been measured. The bending load was carefully increased in 22-kN (5-kip) increments, and blocks of ~10,000 cycles were applied until a compliance change was indicated. The first measurable compliance change occurred after 10,000 cycles at a load range of 22 to 267 kN (5 to 60 kip), which corresponds to a ΔK of 33 MPa $\sqrt{m}$ (30 ksi $\sqrt{in.}$). This load level was maintained, and compliance measurements were made after each increment of 10,000 cycles. A total of 132,000 cycles was required to reach the desired compliance change of 0.046 mm/kN (0.008 in./kip). The second specimen, P3A, was fatigue precracked using the above load range throughout. The desired compliance change was reached in

100,000 cycles. While the number of cycles for crack extension is greater than desired, keeping the ΔK at a level of 33 MPa $\sqrt{m}$ lowers the possibility that a specimen will not undergo rapid crack extension or even fracture during the precracking stage. Subsequent specimens are being precracked under these same load conditions.

The first failure test for this series, specimen P2A, was tested under uniaxial loading at 40°C (104°F), that is, the temperature determined to be NDT from DWTs. The specimen failed by plastic collapse and did not fracture. The load-CMOD history (see Fig. 2.13) illustrates the deformation response recorded. The abrupt upturns in the curves are simply the points where the clip gages lost their grip and fell off the specimen. At a maximum longitudinal load of 1780 kN (401 kip), the test was discontinued because the excessive specimen deformation was endangering elements of the load fixture. The specimen was cut apart for visual examination. The fracture surface is shown in Fig. 2.14. It was observed that the fatigue precracking had resulted in ~2.5 mm (0.1 in.) of crack growth and that the crack had grown another 2.5 mm by ductile tearing during the failure phase of the test. The flaw was then an appropriate depth and was adequately sharpened to have yielded a valid fracture test under the correct conditions. It was concluded that the test temperature was too high for the desired test results. A reevaluation of test conditions and material fracture behavior was undertaken as was discussed in Sect. 2.2.3. The conclusion was that the next test sequence would be performed at temperature of -30°C (-22°F).

Figure 2.13 Load-CMOD history for test of heat-treated plate 14, specimen P2A; load ratio 0:1, test temperature = 40°C (104°F)

Figure 2.14 Fracture surface for cruciform specimen P2A, showing machined flaw, fatigue crack growth, and region of ductile tearing

The second cruciform specimen, P3A, was then tested at -30°C (-22°F) under 0:1 uniaxial loading. Failure by cleavage occurred at a longitudinal load of 736 kN (165 kip). The CMOD and load-line-displacement (LLD) histories for this test are shown in Figs. 2.15 and 2.16, respectively, showing essentially elastic behavior. Toughnesses of $103.0 \text{ MPa}\sqrt{\text{m}}$ ($93.7 \text{ ksi}\sqrt{\text{in.}}$) and $97.2 \text{ MPa}\sqrt{\text{m}}$ ($88.4 \text{ ksi}\sqrt{\text{in.}}$) were calculated from the load-CMOD and load-LLD results. These values are approaching what would be estimated as the lower-shelf toughness for this material, which is consistent with the observed deformation behavior, that is, an elastic test. Examination of the fracture surface indicated initiation near the middle of the flaw front, yielding a valid test. Five additional tests are currently planned at this temperature; one uniaxial (0:1) test, two biaxial (0.6:1) tests, and two biaxial (1:1) tests will be conducted early in the next performance period. These six tests will provide a complete data set for an interpretation of biaxial effects on this material at this temperature. It is noted that analyses indicate that biaxial effects require development of plastic strain on the order of or greater than the elastic strain at the flaw tip. For cases where basically

elastic deformation occurs, biaxial loading has little effect on constraint at the flaw tip and thus little effect on toughness. If the remaining tests in this sequence exhibit low plasticity, then the next sequence of tests will be performed at a higher temperature, where plasticity and toughness would be increased.

2.5 Metallographic Examination of Cruciform Specimen Fracture Surfaces [W. J. McAfee, B. R. Bass, and J. W. Bryson (ORNL); W. L. Fournay and X. J. Zhang (UM)]

Additional work is being performed on fractographic evaluation of the fracture surfaces for selected cruciform specimens from the develop tests. Four fracture surfaces representing the range of biaxial load ratios and toughness values were provided to UM. These specimens and preliminary fractographic results are shown in Table 2.4.

Figure 2.15 Load-CMOD history for test of heat-treated plate 14, specimen P3A; load ratio = 0:1, test temperature = -30°C (-22°F)

Figure 2.16 LLD history for test of heat-treated plate 14, specimen P3A; load ratio = 0:1, test temperature = -30°C (-22°F)

Table 2.4 Summary of results from fracture surface examinations

Specimen (load ratio)	K_{Jc} (ksi $\sqrt{\text{in.}}$)	Z^a (mm)	X_1^b (μm)	X_2^c (μm)	X_3^d (μm)	X_4^e (mm)
BB-11	193	1.4L	240	60	300	10.0
BB-05	177	1.4L	210	90	300	10.2
BB-07	217	8.8R	230	290	520	9.9
BB-10	110	10.1L	90	150	240	11.0

^a Z = the distance [left (L) or right (R)] between the MCIS and the centerline of the specimen.

^b X_1 = the local width of the ductile fracture zone near the MCIS.

^c X_2 = the distance between the MCIS and the end of the ductile fracture zone.

^d X_3 = the distance between the MCIS and the end of the fatigue precrack.

^e X_4 = the distance between the MCIS and the top edge of the specimen.

The fracture surfaces had deteriorated somewhat and required special cleaning to remove an oxide buildup. The locations for the main cleavage-initiation sites (MCISs) were determined, and measurements of the ductile tearing zone were made for the four specimens examined (BB05, BB07, BB10, and BB11). These data are shown in Table 2.4. The results are consistent with measurements made previously on some of these fracture surfaces. Stereo section fractographic studies of these specimens are in progress. The fracture surfaces will then be sectioned near the MCISs and estimates of the size of the local plastic zone will be made.

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* Available for purchase from the National Technical Information Service, Springfield, VA 22161.

† Available in public technical libraries.

3 Evaluation of Cladding Effects

B. R. Bass

3.1 Introduction

During this reporting period, work continued on developing a quantitative description of the effects of cladding on the fracture behavior of shallow surface cracks in RPVs and a methodology for including cladding effects in current RPV integrity assessment procedures.

3.2 Full-Thickness Clad-Beam Testing

Program [B. R. Bass, J. A. Keeney, and W. J. McAfee (ORNL); R. J. Fields and R. deWit (NIST)]

The HSST Program tested full-thickness clad-beam specimens to quantify fracture toughness for shallow cracks in material for which metallurgical conditions are prototypic of those found in RPVs.^{1,2} The beam specimens were fabricated from the Standardized Nuclear Unit Power Plant System (SNUPPS) shell segment that was available from a canceled pressurized-water reactor (PWR) plant (the plant was canceled during construction, and the vessel was never in service). Based on standard procedures, the vessel underwent a postweld heat treatment at ~620°C (1150°F) after the cladding layer was applied. The plate material, clad overlay, and weldment are completely prototypic of a production-quality RPV.

In the initial phase of the investigation, five full-thickness clad-beam specimens (CB-1.1 through CB-1.5) were fabricated from the SNUPPS shell segment at ORNL and tested at NIST, Gaithersburg, Maryland. These tests were performed to investigate the influence of metallurgical gradients, weld inhomogeneities, and the cladding process on the fracture toughness of material containing shallow cracks. Specifically, fracture-toughness data were generated from three-point arc-bend specimens (229- by 226-mm cross section and ~1300-mm chord length) fabricated from full-thickness RPV clad, weld, and plate material. In the first test series, shallow cracks in these beams were located in the longitudinal weld material. Comparison of results from these tests with those from homogeneous shallow-crack test specimens provided an opportunity to quantify the effects of some near-surface conditions on fracture toughness.^{1,2}

In the second phase of the testing program, three additional full-thickness clad-beam specimens (CB-2.1 through

CB-2.3) were fabricated and tested to investigate fracture toughness of shallow cracks located in prototypical full-thickness plate material. Shallow-crack fracture-toughness results from these specimens should provide additional data to better understand the effects of metallurgical conditions in the region of the clad heat-affected zone (HAZ). This section provides a summary of the experimental results obtained from tests CB-2.1 through CB-2.3 performed at the NIST facilities.

3.2.1 Details of Test Specimen

The SNUPPS shell material is A 533 B steel with a stainless-steel clad overlay on the inner surface. The shell segment includes two circumferential welds and one longitudinal weld. The "lower" circumferential weld connects two shell courses, and the "upper" circumferential weld connects a shell course with the nozzle ring. The longitudinal weld is located near the midwidth of the shell segment and runs from the "upper" circumferential weld through the lower end of the shell segment. The welds are submerged-arc welds with A 533 B class 1 filler metal. The plate material, clad overlay, and weldment are prototypic of a production-quality RPV. The shell has a nominal inner radius of 2210 mm (87 in.) and a thickness of 232 mm (9.125 in.), which includes ~5 mm (~0.2 in.) of clad overlay.

Both the first and second series of clad-beam specimens were cut in the circumferential direction of the shell. A longitudinal master blank with a length sufficient to fabricate the test beams was first flame-cut from the shell. The required blanks were then saw-cut from the master blank to ensure that any effects of the flame-cutting would not be contained in the test beams. After saw-cutting, the specimens were machined to final dimensions, incorporating handling, load-contact, and flaw details. The specimen was designed to be tested in three-point bending. Flat, parallel load-contact points were machined on the top and bottom surfaces of the beam to remove surface irregularities and to ensure uniform load application across the width of the beam. A flaw was machined into the beam using the wire EDM process and extended from the shell inner (clad) surface to predetermined depths into the beam. Using the EDM process, flaws with a very narrow width (0.5 mm) and uniform depth can be machined into thick sections with minimum impact on the surrounding material. Heating and the associated potential for introduction of surface residual stresses is minimized, and only small amounts of material are removed. The final dimensions for clad-beam

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specimens CB-2.1 through CB-2.3 are shown in Table 3.1. The crack depth (a) listed for each beam is the final depth after fatigue precracking. Part of the test procedure was to sharpen the flaw by fatigue precracking to achieve ~2.5 mm (0.1 in.) of flaw growth.

Table 3.1 Parameters defining specimen geometry of full-thickness clad-beam specimen

Parameter	CB-2.1	CB-2.2	CB-2.3
a/W	0.1	0.05	0.05
Load span, mm	914	914	914
Thickness, mm	229	229	~229
Width, mm	225	226	~226
Crack depth, mm (fatigue crack)	22.32	10.06	~10.17

Impact and tensile data were generated for the weld, clad, and plate material to develop a consistent set of material properties needed for test data evaluation and finite-element analyses of the clad beams. These data are found in Refs. 1 and 2 and in Sect. 3.3 of this report. The ABI technique was used to determine the yield stress for the plate and clad material, which are given with other material properties in Table 3.2. The tabulated yield stress for the weld material is 36% higher than the yield stress for the plate material. Thus, the weld material exhibits a significant overmatch in yield stress as compared to the yield stress for the plate material.

Table 3.2 Material properties at temperature of -25°C

	Base metal	Weld metal	Cladding
Modulus of elasticity (E), MPa	200,000	200,000	152,000 ^a
Poisson's ratio (ν)	0.3	0.3	0.3
Yield stress (σ ₀), MPa	440 ^b	599 ^c	367 ^b
Ultimate stress (σ _u), MPa	660 ^d	704 ^e	659
RT _{NDT} , °C		-23	
NDT, °C	-34	-50	

^aMeasured E value from S. K. Iskander et al., Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "Experimental Results of Tests to Investigate Flaw Behavior of Mechanically Loaded Stainless Steel Clad Plates," NUREG/CR-5785 (ORNL/TM-11950), April 1992.

^bσ₀ measured by ABI technique.

^cEvaluated from $\sigma_0 = 390 + 51,650/(T + 273)$ where T is the material temperature.

^dσ_u measured by Rockwell B indentation technique.

^eEvaluated from $\sigma_u = 488 + 52,830/(T + 273)$.

3.2.2 Test Equipment and Procedures

The full-thickness clad-beam tests were performed at NIST using the 53.4-MN (12-M lb) servo-hydraulic test machine. A three-point bend fixture with span and load capacity sufficient to perform these tests was designed and fabricated. This fixture was designed for a load capacity of at least 15 MN (3370 kip), which was calculated to be in excess of that required for these tests. Because the tests were performed at a low temperature, an environmental chamber was fabricated to completely enclose the test article and load-contact points to facilitate control of both time- and spatial-dependent specimen temperature variations.

The clad-beam specimens were fully instrumented with CMOD gages, surface strain gages, and thermocouples. The placement of strain gages and thermocouples on beam specimens CB-2.1 through CB-2.3 is shown in Figs. 3.1–3.3, respectively. A specially designed fixture was used to measure LLD. The specimen was then installed in the test fixture, and a careful inspection was performed to confirm that the load-contact points mated properly.

The test sequence for the beams involved two phases: (1) the fatigue precracking phase, and (2) the failure test phase. Figure 3.4 illustrates the test specimen, loading fixture, and the load transfer configuration. For the fracture phase, the manifold for spraying the LN₂ onto the beam surface was mounted around the specimen, and the environmental chamber was used to enclose the assembly. Once the specimen was completely enclosed, it was cooled to the test temperature. A procedure was developed whereby the desired test temperature would be negatively exceeded by ~5°C, the LN₂ spray would be stopped, and the beam would be allowed to thermally equilibrate and warm to the test temperature. This procedure was acceptable because the rate of change in temperature was much less than the time interval required to perform the fracture tests. The specimen was then loaded to fracture under stroke control using a constant LLD rate.

3.2.3 Test Results for Clad Beams CB-2.1 through CB-2.3

A brief description of the test conditions and results for each of the three clad-beam experiments is given below. Essentially the same procedures for fabricating, instrumenting, and testing the beam specimens were employed in all three tests. However, two parameters were varied in this test series, namely, the initial crack depth and the test temperature of the specimen.

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Figure 3.1 Placement of strain gages and thermocouples for test CB-2.1

EFG 96-6757

Figure 3.2 Placement of strain gages and thermocouples for test CB-2.2

EFG 96-6758

Figure 3.3 Placement of strain gages and thermocouples for test CB-2.3

Figure 3.4 Schematic of load system and test beam

3.2.3.1 CB-2.1 Test

A crack-starter notch was spark-machined into the beam specimen to a nominal depth of 19.1 mm prior to delivery of the specimen to NIST. This notch was located in the geometric center of the beam, which was positioned in the center of the test configuration. The notch was fatigue-sharpened at NIST by cyclic loading of the arc-bend chord-

supported specimen, as described in the American Society for Testing and Materials (ASTM) Method E 399. Fatigue cracking was carried out at 20°C using a load ratio of 10. The initial range for the stress intensity factor was 33 MPa√m, followed by a range of 27.5 MPa√m for the final 10% of cyclic loading. Cyclic loading was continued until a fatigue crack was seen extending ~2.5 mm from the end of the spark-machined notch on both sides of the

specimen. This extension was achieved after a total of 18,133 cycles. Loading parameters for the fatiguing process are summarized in Table 3.3. The specimen containing the fatigue-sharpened crack was then instrumented with LLD gages, CMOD gages, thermocouples, and strain gages (see Fig. 3.1).

Table 3.3 Fatigue precracking parameters for full-thickness clad-beam specimens

Parameter	CB-2.1	CB-2.2	CB-2.3
Temperature, °C	20.0	20.0	20.0
$P_{\max}/P_{\min}$	10	10	10
$K_{\max}$, MPa $\sqrt{m}$	33	33	33
ΔK , MPa $\sqrt{m}$	27.5	27.5	27.5
Total cycles	18,133	30,610	18,400

In preparation for the test, the specimen was cooled by LN₂ to a temperature of $-5.2 \pm 1.0^\circ\text{C}$, as measured by thermocouples placed near or on the crack plane (see Fig. 3.1). Then the specimen was loaded at a constant stroke rate of 0.08 mm/s (equivalent to an initial K-rate of 0.464 MPa $\sqrt{m}/s$) until fracture occurred. Within 15 s of the time of fracture, the average temperature in the vicinity of the crack was $-3.6 \pm 1.0^\circ\text{C}$.

After a loading period of 783 s, fracture occurred at a load of 6939 kN, with the load immediately dropping to zero. The beam broke violently into two pieces, sending the specimen halves and support rollers out of the test rig. However, the test setup was not significantly damaged by this result because of precautions taken prior to testing.

Measured data for the applied load vs LLD and CMOD are shown in Figs. 3.5 and 3.6, respectively. These data indicate that a significant amount of plasticity developed in the test specimen. The records for the front and back strain gages (Fig. 3.1), given in Figs. 3.7 and 3.8, respectively, also indicate significant plasticity.

Examination of the fracture surface (Fig. 3.9) revealed that the fatigue crack initiated by ductile tearing, which, in turn, led to cleavage fracture. The crack length at the onset of ductile tearing (i.e., the extent of the fatigue crack) was determined to be 22.32 mm from the nine-point method of ASTM Method E 813. By the same method, the crack length at the onset of fracture (i.e., the extent of ductile tearing) was determined to be 27.97 mm, indicating 5.65 mm of ductile tearing. The rapid fracture surface was rough, exhibited crack branching, and appeared to the unaided eye to be cleavage.

Figure 3.5 Load vs load-line displacement in test CB-2.1

Figure 3.6 Load vs crack-mouth-opening displacement in test CB-2.1

Figure 3.7 Load vs front strain (gages 1-5) in test CB-2.1

Figure 3.8 Load vs back strain (gages 6-10) in test CB-2.1

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Figure 3.9 Fracture surface of bendbar CB-2.1

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3.2.3.2 CB-2.2 Test

A shallower crack (compared to CB-2.1) in the SNUPPS plate material was selected for the second test in this series. Thus, a crack-starter notch was spark-machined into the geometric center of the beam to a nominal depth of 6.4 mm. Cyclic loading of the beams was carried out for 30,610 cycles to extend the crack at least 2.5 mm from the ends of the spark-machined notch. The layout for the thermocouples and strain gages applied to the specimen is shown in Fig. 3.2.

For test CB-2.2, the specimen temperature was reduced below the value used in the previous test, CB-2.1. The specimen was cooled to a temperature of $-33.2 \pm 1.0^\circ\text{C}$ and then loaded at a constant stroke rate of 0.1214 mm/s (equivalent to an initial K-rate of $0.4881 \text{ MPa}\sqrt{\text{m/s}}$) until fracture occurred. Fracture was achieved at 765 s into the loading cycle at a load of 7882 kN and average temperature of $-28.3 \pm 1.0^\circ\text{C}$ in the vicinity of the crack tip. The measured load vs LLD and CMOD curves depicted in Figs. 3.10 and 3.11 indicate the development of significant plasticity in the test specimen. Inspection of the fracture surface (see Fig. 3.12) indicates the fatigue crack

Figure 3.10 Load vs load-line displacement in test CB-2.2

Figure 3.11 Load vs crack-mouth-opening displacement in test CB-2.2

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Figure 3.12 Fracture surface of bendbar CB-2.2

(depth = 10.06 mm) extended 2.52 mm by ductile tearing, that is, to a depth of 12.58 mm, and then initiated in cleavage.

3.2.3.3 CB-2.3 Test

For test CB-2.3, the initial crack depth and test temperature were essentially the same as those used in the second test, CB-2.2. A crack-starter notch having a nominal depth of 6.9 mm was spark-machined into the geometric center of the beam. Cyclic loading of the beam extended the crack ~2.5 mm from the ends of the spark-machined notch after 18,400 cycles. Thermocouples and strain gages were applied to the specimen according to the layout shown in Fig. 3.3.

The specimen was cooled to a temperature of $-34.9 \pm 1.0^\circ\text{C}$ and then loaded at a constant stroke rate of 0.3 mm/min. At 38.55 min into the loading cycle, fracture of the specimen occurred at a load of 7794 kN and average temperature of $-27.6 \pm 1.0^\circ\text{C}$ in the vicinity of the crack tip. The measured load vs LLD and CMOD curves depicted in Figs. 3.13 and 3.14 indicate the development of significant plasticity in the test specimen. The fracture surface (Fig. 3.15) indicates that the fatigue crack, which had initial depth of 10.17 mm, extended 2.68 mm by ductile tearing to a depth of 12.85 mm and then initiated in cleavage. These results are similar to those observed in the previous test, CB-2.2.

Figure 3.13 Load vs load-line displacement in test CB-2.3

Figure 3.14 Load vs crack-mouth-opening displacement in test CB-2.3

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Figure 3.15 Fracture surface of bendbar CB-2.3

Selected test parameters and results for clad-beam tests CB-2.1 through CB-2.3 are summarized in Table 3.4.

Table 3.4 Summary of results from the full-thickness clad-beam testing program

Parameter	CB-2.1	CB-2.2	CB-2.3
a/W	0.10	0.05	0.05
Temperature, °C	-3.6	-28.3	-27.6
Stroke rate, mm/min	4.84	7.28	0.3
Ductile tearing, mm	5.65	2.52	2.68
Time to failure, s	783	765	2313
Failure conditions			
P, kN	6939	7882	7794
LLD, mm	~10	~11.2	~12
CMOD, mm	~4	~2.25	~2.5

3.2.4 Analyses of Clad-Beam Experiments CB-2.1 through CB-2.3

Three-dimensional finite-element models of the clad-beam specimens will be generated and analyzed using the WARP3D fracture-analysis computer program.³ These finite-element models will be constructed to take advantage of the special features contained in WARP3D for modeling the precleavage ductile tearing observed in the three experiments (see Table 3.4). These features include the Gurson-Tvergaard (G-T) dilatant plasticity model^{3,4} for void growth and an element-extinction capability for modeling crack growth. Two principal input parameters for the G-T model are the initial void volume fraction, f_0 , and the characteristic length, D , associated with the G-T crack plane elements. According to theory, these parameters are dependent only on the material and not on specimen

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geometry. To evaluate this model, Task 4 has initiated plans for generating ductile crack-growth data from side-grooved CT specimens taken from the SNUPPS material. The parameters f_0 and D will be calibrated for the material through an iterative process involving finite-element analyses of these CT specimens. The calibrated parameters should provide analytical results in agreement with load vs CMOD and crack-growth data from the CT specimens. Values of these parameters from the CT specimen analyses will then be transferred to the clad-beam models to determine if they predict the response observed in the experiments. Metallography may also provide some estimates of initial porosity and inclusion spacing in the weld, which could be compared with values assigned to parameters f_0 and D .

3.3 Material Characterization of RPV Shell Segment (W. J. McAfee and B. R. Bass)

A series of standard ASTM round-bar tension tests was performed on material taken from the ORNL SNUPPS shell segment. The SNUPPS shell is providing source material for the full-thickness clad-beam and clad-cruciform-beam testing programs being carried out under Subtasks 3.3 and 3.4, respectively. The round-bar test series is intended to provide complete tensile properties, including measured stress-strain curves, for the region in the base material near the clad HAZ of the shell. It is in this region that shallow flaws are located for the beam tests. These tensile tests will provide stress-strain data that are essential to the analyses

of the ongoing uniaxial and biaxial clad-beam testing programs.

Tensile round-bar tests were performed over the temperature range extending from -59 to -4°C (-75 to 25°F). (The NDT for the SNUPPS plate material was determined to be -34°C .) The tensile specimens were taken from two layers located near the clad HAZ. The first layer was 10 mm (0.4 in.) below the top surface of the clad layer; that is, the axial centerline of the machine tensile specimen was located ~ 10 mm below the top surface of the clad overlay. This is approximately the limit of the HAZ for the clad layer. The second layer of specimens was located 23 mm (0.9 in.) below the top surface of the clad layer. This is well within the unaffected base material. It should be noted that these locations also correspond to the crack-tip depths of the shallow-flaw full-thickness clad-beam specimens tested at NIST (see Table 3.1). The test results are summarized in Table 3.5 and in Fig. 3.16. The properties of the HAZ material at the depth tested do not seem to be significantly different from the unaffected base metal, although a slight difference in temperature dependency is observed that is consistent in both yield and ultimate strengths. The material has a stress-strain curve that is characteristic of ferritic steels, a sharp departure from linearity followed by an upper/lower yield region (Luder's bands), and strain hardening to the ultimate strength.

Tests to determine the tensile properties of the weld and of the material in and directly beneath the clad layer of the SNUPPS shell are also being performed. Results of these tests will be available in the next reporting period.

Table 3.5 Summary of SNUPPS base material tensile characterization

Specimen No.	Temperature ($^\circ\text{C}$)	Yield stress (MPa)	Ultimate stress (MPa)	Modulus (GPa)	Uniform elongation (%)	Total elongation (%)	Reduction in area (%)
<i>HAZ: specimen centerline 10 mm below clad surface</i>							
TS01	-4	473	643	237	10.53	26.05	68.6
TS02	-4	484	644	208	10.12	26.67	66.5
TS03	-34	516	686	215	12.23	27.14	66.3
TS04	-34	524	689	204	12.09	27.66	66.9
TS05	-59	527	703	213	11.73	26.46	66.1
TS06	-59	515	697	230	12.47	26.99	66.5
<i>Base metal: specimen centerline 23 mm below clad surface</i>							
TS07	-4	493	652	198	11.09	26.06	66.6
TS08	-4	494	658	201	10.10	24.28	66.0
TS09	-34	509	684	212	10.74	25.77	66.5
TS10	-34	508	682	214	12.15	25.51	62.4
TS11	-59	541	714	211	12.00	26.36	64.2
TS12	-59	530	706	199	11.84	26.59	61.6

Figure 3.16 Tensile strength of SNUPPS shell base material over temperature range NDT - 20°C to NDT + 30°C showing only a small difference in strength of two layers tested

3.4 Testing of Intermediate-Scale Clad Cruciform Specimens (B. R. Bass and W. J. McAfee)

Fabrication of clad cruciform specimens from the SNUPPS shell for use in the intermediate-scale biaxial-fracture-toughness testing program continued during this report period. This testing program will provide information on the influence of both the clad layer and the HAZ on fracture initiation of finite-length shallow flaws in a biaxial stress field. Detailed cut-up plans and fabrication packages were prepared for the SNUPPS shell segments 1 and 2 and were used by vendors to prepare 28 clad-beam specimens. Segment 1 provided material sufficient to fabricate eight intermediate-size clad cruciform beams for the development test matrix. Cut-up of segment 2 provided 16 intermediate-size beams, $102 \times 102 \times 102$ mm ($4 \times 4 \times 4$ in.); four large-scale beams, $152 \times 152 \times 152$ mm ($6 \times 6 \times 6$ in.); and residual material for small specimens and characterization.

Results from initial tests in the series of the intermediate-scale clad-beam specimens will be available in the next reporting period.

3.5 Testing of Large-Scale Clad Cruciform Specimens (B. R. Bass and W. J. McAfee)

The upgrade to the existing 550-kip servo-hydraulic test machine at ORNL was completed during the week of June 12, 1995. The upgraded machine permits testing of larger cruciform-beam specimens under Tasks H.2 and H.3 using an improved load/control capability. (This upgrade subtask is being carried out in conjunction with Task H.2 in the HSST Program). The upgrade consisted of a new electronic controller, a new control computer, software (Labview) for computer control of the machine controller for special test sequences and/or histograms, interface cabling, and calibration of the electronics and load cell to the stall (maximum) capacity of the test machine. Operating the supply system at full pressure, the capacity of the machine is now ~680 kip. Featured improvements of testing machine are summarized in Table 3.6.

In conjunction with the load upgrade, design of a new test fixture was completed during this report period. This test fixture will permit utilization of the full machine load capacity for the intermediate-size clad (H3) and unclad

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Table 3.6 Improvements to ORNL servo-hydraulic testing machine

Parameter	Existing capability	Upgraded capability
Load, kN (kip)	108 (480)	157 (700) ^a
Applied moment, m-kN (in.-kip)		
Uniaxial (0:1)	282 (2500)	989 (8750)
Biaxial (1:1)	136 (1200)	495 (4375)
Beam length, mm (in.)	600 (24)	1420 (56)
Beam depth, mm (in.)	101 (4)	152+ (6+)
Loading mode	Fixed analog ^b	Computer control ^c

^aPotential upgrade to 225 kN (1000 kip).

^bPreset control rate, range, and histogram.

^cComplete computer control using any measured parameter.

(H2) beams being tested. The current fixture is limited to a maximum total load of 480 kip. This new fixture will also permit testing of beams up to 6 in. in cross section and 60 in. long, greatly expanding ORNL capability to investigate different flaw depths and sizes and to test at temperatures above NDT, where ductile tearing is a major contributor to flaw sharpening. Installation of the new test fixture at ORNL is scheduled to take place in the second quarter of FY 1996.

3.6 Probabilistic Assessment of Clad Modeling Effects on Cleavage Initiation in Embrittled Reactor Pressure Vessels (B. R. Bass and T. L. Dickson)

The HSST Program is investigating the effects of the cladding overlay on the propensity for cleavage initiation of shallow flaws on the inner surface of cladged RPVs. Shallow flaws having depths on the order of the combined thickness of the clad and HAZ dominate the frequency distribution of flaws assumed in probabilistic fracture mechanics (PFM) assessments of RPVs. Consequently, the initiation probabilities associated with these flaws under PTS loading conditions can strongly influence the predicted failure probabilities for RPVs. Behavior of these shallow flaws during a PTS transient clearly would be influenced by the material/fracture properties and mechanical interactions associated with the elements (i.e., cladding, HAZ, and near-surface base material) that compose the cladding overlay structure. Thus, coordinated experimental and analytical studies are being conducted within the HSST Program to provide a quantitative description of effects of

the cladding overlay on fracture behavior of shallow surface flaws.

A previous report⁵ described the initial phase of an analytical study of clad modeling effects on predictions of cleavage fracture initiation in an RPV subjected to PTS transient loading conditions. Deterministic analyses performed with the FAVOR fracture-analysis code⁶ were used to predict envelopes of minimum (critical) flaw sizes (depths and lengths) required to achieve cleavage initiation for the prescribed PTS transients. These envelopes, defined in terms of flaw sizes and loading conditions, will be validated with results from the scheduled HSST clad cruciform beam testing program.

During this report period, an analytical study was conducted to determine the effects of clad modeling on probabilistic predictions of cleavage-fracture initiation in an RPV subjected to PTS transient loading conditions. Specific refinements were introduced into the cladding overlay models to take account of a low clad yield stress and a high clad ductility that precludes crack initiation in the clad layer. Probabilistic fracture assessments were carried out with the FAVOR fracture-analysis program on axially oriented finite-length inner-surface flaws with aspect ratios of 2, 6, and 10, as well as for two-dimensional (2-D) flaws.

Changes in the conditional probability of cleavage initiation associated with changes in selected elements of a clad RPV model were determined using PFM techniques. Probabilities were computed for a reference case defined in terms of an ASME-based fracture-toughness correlation, a linear-elastic fracture mechanics (LEFM) model, cladding toughness equal to that of base metal, and a 2-D flaw assumption. Each of the latter elements was then changed separately in the model to determine the corresponding change in conditional probability of initiation for individual flaw depths and for PTS transients of varying severity. Fracture-toughness correlations based on uniaxial and biaxial shallow-flaw toughness data sets were considered, in addition to the ASME-based toughness correlation. The uniaxial shallow-flaw toughness correlation produced initiation probabilities that are 2 to 3 orders of magnitude lower than those from the ASME-based correlation. When the uniaxial shallow-flaw toughness correlation is adjusted for biaxial effects, the resultant correlation provides initiation probabilities that are ~1 order of magnitude lower than those obtained from the ASME-based correlation.

Reductions in probabilities of initiation achieved by combining the effects of semicircular flaw geometry, clad toughness, and clad yield stress vary with flaw depth, but

are more pronounced for shallower flaws. In almost all cases, the minimum average reduction over the range of shallow flaw depths considered herein was ~1 order of magnitude for each of the toughness correlations used in the PFM analyses. The impact of these results on computed probabilities of vessel failure in PTS assessments is strongly influenced by the assumed flaw distribution through the vessel wall. The regulatory requirement that all flaws be treated as surface flaws in PTS evaluations effectively places the flaws at the inner surface, where thermal stresses and irradiation-damage levels are highest. This assumed spatial distribution of the flaw population has been shown to have a dominant influence on the normalized cumulative crack-initiation output curves from PTS analyses of highly embrittled RPVs.

Effects of the cladding overlay structure on shallow-surface-flaw behavior in the transition-temperature region under biaxial-loading conditions will be validated in the testing program described in Sects. 3.4 and 3.5. The testing program will utilize clad cruciform specimens fabricated from prototypic (unirradiated) clad-weld-plate material taken from the SNUPPS shell section. A test matrix incorporating 28 intermediate- and large-scale cruciform test specimens has been defined for the testing program.

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* Available for purchase from the National Technical Information Service, Springfield, VA 22161.

† Available from public technical libraries.

‡ Available in NRC PDR for inspection and copying for a fee.

4 Ductile-to-Cleavage Fracture-Mode Conversion

J. A. Keeney

4.1 Introduction

The objective of Task 4 is to study the fracture process in the transition regime, where crack extension begins in a stable ductile-tearing mode and then converts to a brittle cleavage mode after some amount of crack extension. The material failure by cleavage after ductile tearing can be regarded as a combined effect of a deterministic stable-tearing process (J-R curve) and a probabilistic cleavage process. This ductile-to-brittle mode conversion is associated with large scatter in fracture-toughness testing in the transition-temperature regime. Understanding the fracture-mode conversion will permit a better prediction of actual crack extension in vessels and allow more realistic safety margins to be used in analyses.

During this report period, work continued on the characterization of precleavage tearing behavior and development of a fracture-mode-conversion model to take into account any precleavage ductile tearing in fracture predictions.

4.2 Metallurgical Investigations

(D. J. Alexander, M. C. Rao, and R. K. Nanstad)

The process of precleavage ductile tearing and mode conversion to cleavage is being investigated metallurgically using existing test results. The physical factors involved in this phenomenon have to be accounted for in the development of a fracture-mode-conversion model. Also, different fractographic features are important depending on the model that is used, for example, void and inclusion volume fraction for void formation and coalescence models. The steps of this investigation will include (1) identifying potential sources of fracture surfaces from ORNL that are relevant to precleavage ductile tearing, (2) securing the fracture surfaces that are from other sources besides ORNL, and (3) conducting examinations of the fracture surfaces. The initial metallurgical examination will (1) characterize the extent of crack-tip blunting prior to crack growth, (2) identify and characterize the microstructural features associated with microvoid formation and growth, (3) identify and characterize the microstructural features responsible for the mode conversion to cleavage, and (4) describe the relationship between the ductile crack morphology and the trigger points at mode conversion. The results from these investigations will be used in the development of the fracture-mode-conversion model.

The first step of this project was to select a material and measure the metallurgical features that might be involved in the fracture-mode-conversion process. This would demonstrate the capabilities of the characterization facilities and provide an initial set of parameters that might be involved in any modeling of the material. These microstructural features would be incorporated in the appropriate constitutive equations and models used to examine analytically the mode-conversion process. A letter report that describes the initial characterization of the first candidate material, "Preliminary Metallurgical Investigations of Candidate Material for Ductile-to-Cleavage Fracture Mode Conversion Study" (ORNL/NRC/LTR-95/9) by D. J. Alexander, M. C. Rao, and R. K. Nanstad, will be published.

The material selected for the initial characterization was the weld metal from a canceled nuclear RPV.¹ The inclusions present in the weld metal were characterized on specimens that were metallographically polished and examined in a scanning electron microscope (SEM) by quantitative image analysis to characterize their size distribution and spacing.

The size distribution from the metallographic measurements was compared to that determined from fractographic examination of an actual fracture surface. Particles on the fracture surface were examined on a Charpy specimen that exhibited ductile tearing before final failure by cleavage fracture. The fracture surface at the transition from ductile dimples to cleavage was examined in the SEM. The average of the largest and smallest dimensions of the particles was used as the particle size.

The results of this fractographic measurement of the particle size are similar in nature to the metallographic measurements, but the particles are significantly larger. The mean particle size was 0.87 μm with a standard deviation of 0.41 μm , as compared to the values from the metallographic examination of 0.50 μm for particle size with a standard deviation of 0.27 μm .

There are several possible reasons for the particle size calculated from the polished sections to be significantly smaller than that observed on the fracture surface. The polished sections will sample all particles, and thus all particles down to the resolution limit of the SEM will be counted and analyzed. However, the fracture process

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probably begins at the larger particles and involves the smaller particles at only the final stages of fracture, if at all. If the larger particles are plentiful, fracture may be completed without the smallest particles' being needed. Thus, the fracture surface gives a truer indication of which particles are actually participating in the fracture process and so should provide a better sampling technique for characterizing the relevant particle population.

One possible complication is that the testing conditions may affect the population of particles that are involved. The stresses and strains at the crack tip may have an effect on which particles participate in the fracture process, and the stresses and strains may be different at different test temperatures and strain rates, because of the effect of these parameters on the yield stress and work-hardening characteristics of the material. Perhaps the best solution is to determine the size distribution by examining the near-tip region of the fracture surface of the actual test specimen that is to be modeled. This will guarantee that the relevant population is being measured.

Material is not available from the inner portion of the longitudinal weld of the SNUPPS shell, which is the region of relevance for the full-thickness clad-beam tests. Materials from the circumferential weld and the outer portion of the longitudinal weld have been provided for characterization, and it will be assumed that these materials are the same as the inner portion of the longitudinal weld. For the circumferential weld, only fracture-toughness tests will be conducted, because Charpy, tensile, and drop-weight

tests have already been conducted.¹ Compact, tensile, CVN, and drop-weight specimens from the outer portion of the longitudinal weld will be tested for comparison with the circumferential weld. Material adjacent to the outer portion of the longitudinal weld will be used for the base metal specimens. Compact specimens will be prepared for fracture-toughness testing at room temperature. The pieces of material containing these two welds have been sent out for fabrication of specimens.

4.3 Fracture-Mode-Conversion Model

Development (B. R. Bass and J. A. Keeney)

4.3.1 Slab-Type Gurson Model

Shih and Xia^{2,3} and Ruggieri and Dodds* have idealized the void-growth mechanism in a model that assumes that void growth and coalescence are confined to a narrow material layer of initial thickness D , which is centered about the macroscopic fracture plane (see Fig. 4.1). They justify this simplification on the basis that many metals that fail by void growth and coalescence exhibit a macroscopically planar fracture process zone of one or two void spacings in thickness. Ligaments between the voids experience intense plastic deformation. Voids outside this region are observed to display little or no growth. Within the fracture

*C. Ruggieri and R. H. Dodds, "Numerical Modeling of Ductile Crack Growth for Porous Plastic Materials in 3-D Using Computational Cells," University of Illinois, to be published.

Figure 4.1 Ductile tearing by hole growth and coalescence studied with computational tools, including local-damage models (Gurson) and highly refined ($\sim 100\text{-mm}$) crack-tip meshes

process zone layer of thickness D , the material response is described by a simplified Gurson micromechanical model (SGMM). Outside this layer, the material is assumed free of voids, and the response to deformation is described by classical J_2 flow theory of plasticity. The overall objective of this approach is to develop an engineering micro-mechanics model that embodies the essence of stress tri-axiality on fracture toughness, while requiring only a few essential metallurgical parameters.

4.3.2 Computer Program Development

(R. H. Dodds, Jr., UI)

Ruggieri and Dodds* have incorporated the SGMM into the finite-element computer program WARP3D,⁴ which is under development at UI. This research code analyzes very large three-dimensional (3-D) solid models encountered in fracture-mechanics studies of crack-tip fields and ductile crack growth. Currently, work has been centered on the numerical modeling of ductile fracture in 3-D. In this work, two mechanisms are being investigated for modeling 3-D ductile crack growth using finite elements: the computational cells and node release. Both methods are implemented in the research code WARP3D, with the node-release approach implemented as part of this work. The computational cell model predicts the growth and coalescence of voids in material ahead of crack tip using a G-T constitutive model for dilatant plasticity. When voids within an element grow to a critical size, the processors for crack growth eliminate the element from the model, thereby growing the crack by a discrete increment of the element size. The node-release model grows the crack by removing constraints applied to nodes on the crack plane when local deformation levels reach a critical value; in this case, the measure of deformation is the opening angle at the crack tip. The implementation of this method in WARP3D is similar to traditional implementations, but also includes fully automatic facilities to support growth along multiple 3-D crack fronts.

Several example analyses illustrate applications of the numerical algorithms as implemented in WARP3D for crack growth. Analyses of plane-strain SENB models, $a/W = 0.14$ and 0.6 , demonstrate the potential for each approach to match experimental R curves. The computational cell approach, however, predicts correctly the crack growth for geometries other than the one used for calibration of the cell parameters. As expected, the node-release method reveals a strong dependence of the critical crack-tip-opening angle (CTOA) on constraint changes across specimen geometries. It is clear that constraint-dependent

calibrations of the specified critical CTOA values are required for successful predictions of measured R curves. Other example analyses demonstrate the potential flexibility of the node-release approach in 3-D: (1) an initially rectangular flaw embedded inside a bar grows under uniaxial loading to a circular shape, and (2) tunneling is predicted correctly in a non-side-grooved 3-D model of an SE(B) specimen.

The WARP3D software for 3-D nonlinear analyses of fracture-mechanics specimens and structural components has been successfully ported to the 16-processor IBM SP2 high-performance parallel system located at ORNL. Each of the 16 RISC/6000 POWER2 66.5-MHz processors contains 128 MB of memory plus a 1-GB disk. The processors are in two frames and are connected by a high-performance switch and an Ethernet. This system has a peak performance of 4.25 Gflops. Twelve of the nodes are "thin" nodes, and four are "wide" nodes. Wide nodes are more expandable, allow more input/output options, and have larger caches and wider databases. The SP2 system will provide a more powerful tool for the development of scientific software for execution on more powerful parallel platforms.

Comparisons of run time for identical WARP3D input data sets were performed between the RISC/6000 workstation (560) and a single node of the IBM SP2 high-performance parallel system located at ORNL. Both machines gave the same answer, but the SP2 used half the processing time that the RISC/6000 workstation required. Work is currently under way to modify the WARP3D code to run on the SP2 as a parallelized code, which should further reduce the run time of a job.

Also during this report period, modifications were made to the Dodds-Anderson (D-A) toughness-scaling model to render it applicable to biaxial loading conditions. The original development of the D-A model assumed the presence of sufficient plastic strain over the cleavage process zone to provide nucleation sites. For conventional fracture specimens loaded in uniaxial bending and uniaxial tension, plastic strain and crack-region stresses increase in roughly proportional fashion, generally with large plastic strains developing over the cleavage process zone early in the loading. This may explain the relatively good toughness correlations obtained for the D-A model for these conventional specimens.

Currently, the Weibull stress approach has been extended for predicting size effects on cleavage fracture to include the effects of ductile crack extension.⁵ The ductile crack

*C. Ruggieri and R. H. Dodds, "Numerical Modeling of Ductile Crack Growth for Porous Plastic Materials in 3-D Using Computational Cells," University of Illinois, to be published.

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extension is modeled by the computational cell approach. Initial analyses in plane strain for small-scale yielding with varying levels of T stress and for deep and shallow SENB specimens have been completed. Predictions of toughness distribution of shallow-notch data from measured deep-notch data agree well with experimental results.

4.3.3 Calibration of Simplified Gurson Model

In Task H.4, initial studies of ductile-tearing models have focused on the SGMM for void growth and an element-extinction capability for modeling crack growth. These special features for modeling precleavage ductile tearing in pressure vessel steels are contained in the WARP3D finite-element computer code.⁴ Using the SGMM, void nucleation and growth ahead of a stably tearing crack are incorporated into a finite-element model using computational cells with explicit length scales, as illustrated in Fig. 4.2. Two principal input parameters for the SGMM are the initial void volume fraction, f_0 , and the characteristic length, D , associated with SGMM crack-plane elements. According to theory, these parameters are dependent only on the material and not on specimen geometry. To evaluate this model, Task 4 is in the process of generating ductile crack growth data (R curves) from side-grooved CT specimens taken from the ORNL SNUPPS material. The parameters f_0 and D will be calibrated for the material through an iterative process involving finite-element analyses of these CT specimens (see Fig. 4.3). The calibrated parameters should provide analytical results in agreement with load vs CMOD and crack-growth data from the CT specimens. Values of these parameters from the CT specimen analyses will then be transferred to large-scale structures (e.g., the full-thickness clad beams) to determine if they predict the observed response. Metallography may also provide some estimates of initial porosity and inclusion spacing in the weld, which could be compared with values assigned to parameters f_0 and D .

Posttest analyses of the full-thickness clad-beam test CB-1.5, which experienced precleavage ductile tearing, are being performed (in conjunction with Task 3) utilizing the WARP3D fracture-analysis computer program. In the absence of experimental $J - \Delta a$ curves for the weld material, a parametric study was performed using the experimental load vs CMOD data from CB-1.5. This study was carried out to determine the effect of f_0 and step increment size on (1) CMOD, (2) internal forces, and (3) crack growth as modeled by the number of elements that become extinct (lose their load-bearing capability). Plane-strain finite-element analyses² of SENB and CT specimens used to estimate the Gurson parameters for structural and pressure vessel steels indicate values of 50–200 μm for D , 0.001–0.005 for f_0 , and 0.15–0.20 for f_c . The experimental value of f_0 for pressure vessel steel (A 533 B) is ~ 0.0004 , but to capture the effects of highly localized zig-zag fracture, the value of f_0 used in analyses is 5–15 times greater than the experimental value. The measured value of f_0 for the weld material is 0.004 (an order of magnitude larger than the pressure vessel steels).⁶ The value of f_0 used in the parameter study ranged from 0.006 to 0.01, and the displacement increment ranged from 0.00215 to 0.0015. The explicit length scale D was set at 200 μm (because the crack-tip element size is 100 μm) and the critical volume fraction at $f_c = 0.15$. To match the measured CMOD for CB-1.5, the load had to be increased by 12%, as shown in Fig. 4.4. A higher load had to be used because the 3-D plane-strain model [one layer of elements in the thickness direction with plane-strain constraints ($z = 0$) imposed on all nodes] may be too stiff for an exact comparison between measured and calculated P vs CMOD.

During the next report period, the generation of R curves from side-grooved CT specimens taken from the SNUPPS material will be completed, and the parameters f_0 and D will be calibrated.

Figure 4.2 Two material damage parameters, initial slab height D and initial volume fraction f_0 , selected to give best fit to experimental crack-growth data

Figure 4.3 Small laboratory specimens used to calibrate Gurson model parameters f_0 and D

Figure 4.4 Comparison of experimental and calculated CMOD values for CB-1.5

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* Available for purchase from the National Technical Information Service, Springfield, VA 22161.

† Available in public technical libraries.

‡ Available from organization sponsoring the cited publication, and/or from authors, and/or recipients.

5 Fracture Analysis Methods Development and Applications

T. L. Dickson and J. Keeney

5.1 Development of Stress-Intensity-Factor Influence Coefficients

Work continued on the generation of SIFICs. This is a continuation of an ongoing task that has the objective of developing a comprehensive database of accurate validated SIFICs for a range of inner-surface-flaw geometries that will envelope the commercial PWR and boiling-water reactor (BWR) vessel geometries in the United States. ABAQUS,¹ a nuclear quality assurance certified (NQA-1) multidimensional finite-element code with fracture-mechanics capabilities, is being used to generate the SIFICs. The incorporation of this SIFIC database into the FAVOR computer code² will facilitate the generation of accurate fracture-mechanics solutions for a range of flaw geometries such as may be required in the structural integrity assessments of any U.S. BWR or PWR geometry.

During previous reporting periods, letter reports have been published that reported SIFICs for axially and circumferentially oriented inner-surface infinite-length³ and semielliptical⁴⁻⁵ flaws in clad pressure vessels that have an internal radius-to-wall thickness ratio (R_i/t) ratio of 10. A technical paper⁶ was published and presented during this period for the purpose of validating the FAVOR K_I solutions that utilize the SIFIC database. FAVOR K_I solutions were validated by comparing the FAVOR solutions to ABAQUS direct finite-element solutions. A set of benchmark validation problems for various flaw geometries was developed. The FAVOR solutions, which utilize the SIFIC database and superposition to calculate K_I 's, was within 1 to 2% of the ABAQUS 3-D solutions.

During this reporting period, modifications were made to ORMGEN,⁷ the finite-element mesh generator to more accurately model the elliptical flaw geometry. In some cases, this model refinement significantly affected the previously published SIFICs. The refined SIFICs were published⁸ and incorporated into FAVOR during this reporting period.

Also during this reporting period, work was completed on generating stress SIFICs for axially oriented semielliptical flaws with aspect ratios (ratio of total crack length to crack depth) of 2, 6, and 10 that reside on the inner surface of clad RPVs with an R_i/t ratio of 20. The exact dimensions of the RPV being used in the models to generate the SIFICs is an internal radius of 126.5 in., a wall thickness of 6.125 in.,

and clad thicknesses of 0.156 and 0.25 in.. These dimensions are prototypical of a large percentage of the commercial BWRs in the United States.⁹ During the next reporting period, SIFICs will be generated for circumferentially oriented semielliptical flaws with aspect ratios of 2, 6, and 10 that reside on the inner surface of clad RPVs with an R_i/t ratio of 20.

The SIFICs for clad vessels with an R_i/t ratio of 20 will be validated in the future by implementing them into FAVOR. The FAVOR solutions, which utilize the SIFICs and superposition, will be compared against ABAQUS direct 3-D solutions (milestones H.5.1.b.1 and b.2).

5.2 FAVOR

5.2.1 Review of PTS Screening Criteria—Examine the Impact of Various Fracture-Model Modifications

The NRC is currently evaluating the possibility of revising current PTS regulations. Technical bases must be developed to support any revisions. A thorough review of SECY-82-465¹⁰ is required to understand the origins of the current PTS screening criteria.¹¹

The PTS screening criteria specified in SECY-82-465 were derived from both deterministic and probabilistic analyses. The OCA code¹² (developed at ORNL) was used to perform the deterministic fracture analyses in SECY-82-465. The VISA code¹³ (developed by NRC and Pacific Northwest Laboratory) was used to perform the PFM analyses in SECY-82-465.

During a previous reporting period, the deterministic and PFM analyses from which the current PTS screening criteria were derived were successfully reconstructed using the FAVOR fracture-analysis computer code (milestone H.5.2.c.1).

The methodology utilized in SECY-82-465 to derive PTS screening criteria from probabilistic considerations was to determine the value of RT_{NDT} s corresponding to a specified acceptance criterion. The NRC specified the acceptance criterion to be a maximum acceptable frequency of crack extension due to PTS of 5.0×10^{-6} per reactor year.

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It should be noted that the term "crack extension," used in SECY-82-465, is understood to mean a through-wall crack penetration.

In SECY 82-465, the frequency of crack extension is specified to be the sum of the products of the transient frequencies of occurrence and the conditional probabilities of crack extension for the postulated PTS transients. Transient frequency of occurrence is expressed in PTS events per reactor year. The conditional probability of crack extension in a RPV is determined by performing a PFM analysis and is expressed as crack extensions per PTS event. The conditional probability of crack extension is conditional in the sense that the transient is assumed to occur. Therefore, the frequency of crack extension is expressed as the number of crack extensions per reactor year.

The PFM analyses were performed for postulated transient sequences obtained from Westinghouse Owners Group. The total frequency of crack extension corresponding to the NRC-specified acceptance criterion of 5×10^{-6} per reactor year is almost exclusively due to the extended HPI transient. Therefore, to reconstruct the PFM analysis results from which the current PTS screening criteria were derived, it was necessary only to duplicate the results for the extended HPI PTS event. In SECY 82-465, the extended HPI PTS transient was characterized stylistically

by $T_f = 125$ F; $B = 0.05 \text{ min}^{-1}$, and a constant pressure of 2250 psi. The extended HPI PTS transient is illustrated in Fig. 5.1. This transient was determined to have an frequency of occurrence of 1×10^{-4} PTS events per reactor year; therefore, the corresponding conditional probability of crack extension that would produce a frequency of crack extension of 5×10^{-6} is 5×10^{-2} .

Figure 5.2 illustrates the PFM solutions from which the PTS screening criteria were derived, as generated by VISA and as reconstructed by FAVOR. As can be seen, the FAVOR PFM analyses results are nearly identical to the solutions from which the current PTS screening criteria were derived. The value of RT_{NDT} s corresponding to a probability of crack extension of 5×10^{-2} is $\sim 210^\circ\text{F}$. Adding a 2σ value (for the uncertainty associated with predicting RT_{NDT} s) of 60°F yields the PTS screening criteria of 270°F .

The FAVOR PFM model will serve as a baseline model for future analyses that will examine the impact of various fracture-model variations on the PTS screening criteria. During this reporting period, PFM analyses were performed to examine the impact on the current PTS screening criteria of various fracture-model modifications (milesone 5.2.c.2).

Figure 5.1 HPI extended transient, stylized characterization

Figure 5.2 VISA and FAVOR PFM analysis results for HPI PTS transient, from which current PTS screening criteria derived

5.2.2 Impact on PTS Screening Criteria of Reconciling SECY-82-465 PFM Model with Regulatory Guide 1.154 PFM Model

There are several inconsistencies between the PFM modeling assumptions used in SECY-82-465 and those currently specified in Regulatory Guide (RG) 1.154.¹⁴ The differences are tabulated in Table 5.1. It should be noted that in RG 1.154 some modeling assumptions are explicitly specified and others are implicitly specified by endorsing the techniques used in the integrated pressurized-thermal-shock (IPTS) studies¹⁵⁻¹⁷ as acceptable.

Fracture-model modifications were incrementally made to the PFM base case model used in SECY-82-465 to make the PFM model consistent with the model specified in RG 1.154. PFM analyses were performed on these models to determine the impact on the current PTS screening criteria. Table 5.2 contains the results of these analyses.

PFM model 2 quantifies the impact of the inclusion of clad. RG 1.154 explicitly specifies that the clad region should be included in both the thermal and stress analyses. In the SECY-82-465 analysis, the clad region was included in the thermal analysis; however, it was not included in the stress analysis. This approach takes advantage of the insulating

property of clad (by including the lower thermal conductivity of clad relative to that of the base material) while ignoring the liability of the discontinuity stress associated with the clad because of its higher value of thermal expansion coefficient. The impact of including the clad discontinuity stress is to reduce the PTS screening criteria by 13°F.

PFM model 3 quantifies the impact of using the methodology in RG 1.99, Rev. 2 (Ref. 18) to calculate the RT_{NDT} . In the SECY-82-465 analyses, the Hanford Engineering Development Laboratory (HEDL) RT_{NDT} shift correlation was used. This has no impact on the PTS screening criteria.

PFM model 4 quantifies the impact of using 0.24 for the neutron fluence through-wall attenuation constant and applying 1- σ values of 24°F and 17°F used to represent the uncertainties in the RT_{NDT} shift correlation and $RT_{NDT,0}$ respectively. This modification reduced the PTS screening criteria by 10°F.

PFM model 5 quantifies the impact of using the ASME-based fracture-initiation-toughness curve. The IPTS studies referenced by RG 1.154 assumed that the ASME K_{IC} curve was the mean -2 σ curve and assumed a 1 σ of 0.15 times the mean value. K_{IC} values were stochastically simulated between +3 σ and -3 σ . This PFM model modification resulted in a 32°F reduction in the PTS screening criteria.

Table 5.1 Inconsistencies between the PFM models utilized in SECY-82-465 and RG 1.154

	SECY-82-465	RG 1.154 ^a
K_{Ic} correlation	NRC-derived	ASME-based
ΔRT_{NDT} correlation	HEDL	RG 1.99 Rev. 2
Flaw-depth distribution	OCTAVIA	Marshall
Neutron fluence attenuation constant	0.33	0.24
1 σ used to represent uncertainty in		
RT_{NDT} shift correlation	0°F	24°F
RT_{NDT0}	15°F	17°F
Clad	Included in thermal analysis	Include in thermal and stress analyses
Regions in model	6 axial welds	Entire beltline, including axial welds, circumferential welds, and plates regions
Flaw density	~1/ft ³	1/m ³

^aSome fracture-modeling assumptions are explicitly specified in RG 1.154. Others are implicitly specified by referencing the methods utilized in the IPTS studies as acceptable.

Table 5.2 Impact on PTS screening criteria of reconciling SECY-82-465 PFM model with RG 1.154

Model No.	Model description	Screening criterion (°F)	Model change (°F)	Cumulative model changes (°F)
1	Base	208		
2	Includes clad discontinuity stress	195	-13	-13
3	Model 2 + RG 199 Rev. 2	195	0	-13
4	Model 3 +	185	-10	-23
	(a) 1 σ = 24°F for RT_{NDT} shift correlation (base model uses 0°F)			
	(b) 1 σ = 17°F for RT_{NDT0} (base model uses 15°F)			
	(c) Fluence attenuation decay constant = 0.24 (base model uses 0.33)			
5	Model 4 +	153	-32	-55
	ASME-based K_{Ic} curve			
	1 σ = 0.15 (K_{Ic}) mean			
6	Model 5 + Marshall flaw-size distribution function	112	-41	-96
7	Model 6 using a flaw density of 1/m ³	205	+93	-3
8	Model 7 + two circumferential flaws (8 ft ³) with flaw density of 1/m ³	193	-12	-15
9	Model 8 + 388 ft ³ of base metal with flaw density of 1/m ³ (plate same chemistry and RT_{NDT0} as welds)	103	-90	-105
10 ^a	Model 9 +	150	+47	-58
	Weld chemistry: Cu = 0.25%			
	Ni = 0.65%			
	Plate chemistry: Cu = 0.118%			
	Ni = 0.571%			
	Weld RT_{NDT0} = -56°F			
	Plate RT_{NDT0} = 5.5°F			

^aModel 10 is the same as model 9 except that it includes a generic RPV beltline neutron-fluence map and has more realistic chemistries and initial unirradiated RT_{NDT} (RT_{NDT0}).

PFM model 6 quantifies the impact of using the Marshall flaw-depth distribution function. PFM analyses were performed using the Marshall flaw-depth distribution function (without inspection) with 15 discrete flaw depths, evenly spaced in the first 2 in. of the RPV wall. The impact of the application of the Marshall flaw-depth distribution function was to further reduce the PTS screening criteria by 41°F.

PFM model 7 quantifies the impact of using a flaw density of $1/\text{m}^3$. In the SECY-82-465 analyses, only the axial weld regions were included in the model. The model did not include circumferential welds or plate regions. Each of the six axial welds was assumed to have exactly one flaw; therefore, the model contained exactly six flaws, implying a flaw density of $\sim 1/\text{ft}^3$. RG 1.154 specifies that the entire RPV beltline region should be considered in the analysis and that an acceptable flaw density is $1/\text{m}^3$. The impact of reducing the flaw density is to increase the PTS screening criteria by 93°F.

PFM model 8 quantifies the impact of including 8 ft³ of circumferential weld with a flaw density of $1/\text{m}^3$. The circumferential weld material is at the same level of embrittlement at the axial welds. The impact of including the circumferential welds is to decrease the PTS screening criteria by 12°F.

PFM model 9 quantifies the impact of including 388 ft³ of plate material with a flaw density of $1/\text{m}^3$. The plate material is at the same level of embrittlement as the axial and circumferential welds. The impact of including the circumferential welds is to decrease the PTS screening criteria by 90°F.

PFM model 10 quantifies the impact of making the analysis more prototypical, that is, the inclusion of a detailed neutron-fluence map, plate chemistries that have lower copper and nickel contents than the weld material, and prototypical distinct values for weld and plate RT_{NDT_0} . PFM models 1–9 assume that all beltline regions included in the model have a single value of embrittlement; that is, the same values of RT_{NDT_0} , copper, nickel, and neutron fluence are assigned to all regions in the PFM model. PFM models 10–15 include a prototypical, detailed neutron-fluence map. The map divides the entire RPV beltline region into discrete subregions, each having its own value of neutron fluence, for each 3° azimuthal variation and each 6-in. axial variation. The impact of the modifications in PFM model 10 is to increase the PTS screening criteria by 47°F.

The conclusion is that if the PFM analyses, from which the PTS screening criteria were derived, were performed on a

prototypical RPV using the PFM model as currently specified in RG 1.154, the PTS screening criteria would be substantially reduced (by 58°F).

5.2.3 Impact on PTS Screening Criteria of Various Modifications to the RG 1.154 PFM Model

Table 5.3 contains the results of incrementally modifying the PFM model as currently specified in RG 1.154 (model 10).

PFM model 11 quantifies the impact of increasing the variability of embrittlement-related parameters input into the Monte Carlo simulations. The standard deviations as specified in Table 5.3 for model 11 are generic values, derived by the NRC, from their Reactor Vessel Integrity Database (RVID).^{*} The impact is to decrease the PTS screening criteria by 30°F.

PFM model 12 quantifies the impact of stochastically simulating flaw aspect ratio. All previous analyses have assumed all flaws to be infinite in length. In this and subsequent PFM analyses, flaw aspect ratio is sampled from a uniform distribution of 2, 6, 10, and infinity. The impact is to increase the PTS screening criteria by 17°F.

PFM model 13(a) quantifies the impact of including an approximate method for including clad plasticity into the PFM analysis. The method is simply not to let the stress in the clad region exceed 55 ksi. The impact is to increase the PTS screening criteria by 5°F.

PFM model 13(b) quantifies the impact of the application of the K_{IC} database from the shallow flaw-biaxial experimental database. The derivation of the K_{IC} correlation to include shallow flaw-biaxial effects is discussed in detail in Ref. 19. The derivation was based on a rather limited experimental database at the time of these analyses. Therefore, the impact of shallow flaw-biaxial K_{IC} could change considerably as the database is enlarged. The impact is to increase the PTS screening criteria by 73°F to 215°F.

PFM models 14 and 15 quantify the impact of assuming a plate-flaw density to be $0.1/\text{m}^3$. The impact is to increase the PTS screening criteria by 33°F and 50°F.

^{*} Communication from S. N. M. Malik, NRC, Office of Nuclear Regulatory Research, Materials Engineering Branch.

Table 5.3 Impact on PTS screening criteria of various modifications to RG 1.154 PFM model^a

Model No.	Model description	Screening criterion (°F)	Model change (°F)	Cumulative model changes (°F)
11	Model 10 + some new definitions for 1 standard deviation: Weld chemistry: Cu = 0.07% Ni = 0.16% Plate chemistry: Cu = 0.051% Ni = 0.062% Weld RT _{NDT0} = 17°F Plate RT _{NDT0} = 22.5°F Weld ΔRT _{NDT} = 28°F Plate ΔRT _{NDT} = 17°F	120	-30	-88
12	Model 11 + sample flaw aspect ratio (assumed uniform distribution of aspect ratio 2, 6, 10, and infinity)	137	+17	-71
13a	Model 12 + includes clad plasticity (clad stress limited to 55 ksi)	142	+5	-66
13b	Model 13a + uses shallow flaw K _{IC} database adjusted for biaxial effects ^b	215	+73	+7
14	Model 13a + uses plate flaw density of 0.1 flaw per cubic meter	192	+50	-16
15	Model 13b + uses plate flaw density of 0.1 flaw per cubic meter	248	+33	+40

^aNote: In all analyses (K_{IC})_{clad} = (K_{IC})_{base}.

^bThe methodology used to incorporate shallow flaw-biaxial effects on fracture-initiation toughness (K_{IC}) in these PFM analyses is as specified in Ref. 19.

The conclusion is that the application of the current K_{IC} database from the shallow flaw-biaxial experimental results and a lower flaw density for the plate region substantially increase the PTS screening criterion. The inclusion into the PFM model of stochastically sampling flaw aspect ratio and an approximate method for including clad plasticity also increase the PTS screening criteria, although to a much lesser extent.

When the SECY-82-465 PFM analysis, from which the current PTS screening criteria were derived, is performed using the PFM model specified in RG 1.154 as modified as discussed above, the PTS screening criteria are not more limiting than the current value.

It is anticipated that the letter report entitled "Review of the Pressurized-Thermal-Shock Screening Criteria for Embrittled Pressurized-Water Reactor Pressure Vessels" will be published during the next reporting period. This report will contain all of the details regarding the analysis assumptions and results.

5.3 NRC Support

T. L. Dickson made a presentation at the NRC/NEI Workshop on Reactor Pressure Vessel Integrity entitled "The Impact of Fracture Model Variations on PTS Screening Criteria." He also made a presentation in Honolulu, Hawaii, at the 1995 ASME Pressure Vessel and Piping Conference entitled "Validation of FAVOR Code LEFM Solutions for Finite Length Flaw Geometries." A paper of the same title, authored by Dickson, J. Keeney, and J. W. Bryson was published in the proceedings of the conference.

Dickson prepared and made the following presentations at the EPRI PTS Workshop in Scottsdale, Arizona: "Crack Arrest Effects on the Conditional Probability of Through-Wall Cracking" and "Example Applications of FAVOR (release 9501): A Computer Code for Reactor Pressure Vessel Integrity Assessment." The second of these presentations consisted of several example applications of the FAVOR code for performing deterministic and probabilistic fracture analyses of embrittled nuclear RPVs subjected to transient conditions such as P-T shock. Dickson

distributed a FAVOR (release 9501) installation disk to all workshop participants. The installation disk contained the executables and several example input data sets.

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^{*} Available in NRC PDR for inspection and copying for a fee.

[†] Available from public technical libraries.

[‡] Available for purchase from the National Technical Information Service, Springfield, VA 22161.

^{**} Copies are available from U.S. Government Printing Office, Washington, D.C. 20402. ATTN: Regulatory Guide Account.

6 Material Property Data and Test Methods

R. K. Nanstad

6.1 J-R Curve Evaluations on A 302 Grade B Steel (D. E. McCabe)

The writeup of the final NUREG report, "Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials, Data Analysis," has been completed.* The above work and title apply to the first of the two volumes that are planned. It has been prepared for general distribution because it contains the information that defines and reports the overall objectives of the project. Volume 2 will be a data documentation report to preserve data for possible future use. Volume 1 presents both baseline material characterization data and fracture-mechanics-based J-R curve data. Seven heats of modified A 302 grade B steel and one early vintage heat of A 533 grade B steel were tested. Test condition variables were test temperature [82, 204, and 288°C (180, 400, and 550°F)]; crack-plane orientation (longitudinal, transverse, and short transverse); and specimen size (1/2T, 1T, 2T, and 4T compacts). Single-value parameters that can be used to compare J-R curves are J_{Ic} at onset of ductile tearing and J after 2.5 mm (0.10 in) of stable crack

growth. These properties as well as tensile properties and CVN upper-shelf energies (USEs) are tabulated.

The primary objectives were to (1) determine whether the J-R curve fracture toughness is section size-dependent, (2) develop some generic fracture-toughness data on modified A 302 grade B steel for potential use in analyses applications, (3) determine typical orientation effects and test temperature effects, and (4) develop generic CVN and drop-weight NDT data. Over 230 compact specimens of sizes ranging from 1/2T to 4T, 285 CVN, 65 tensile, and 64 drop-weight NDT specimens were tested.

It was determined that the J-R curve toughness of modified A 302 grade B steel of typical production practice does not degrade with increased specimen size. The results from an earlier Materials Engineering Associates (MEA) experiment that showed a significant loss in J-R curve toughness with increased specimen size were not confirmed here. In fact, the fracture toughness of the MEA plate of A 302 grade B was lower than that of any of the modified A 302 grade B plates tested in this study, and no significant specimen-size effect was observed in materials of improved toughness. Figure 6.1 compares the typical J-R

*D. E. McCabe, E. T. Manneschildt, and R. L. Swain, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials, Data Analysis," Vol. 1, USNRC Report NUREG/CR-6246, to be published.

Figure 6.1 J-R curve and data for modified A 302 grade B vs plate V50 J-R curves and data of MEA tests

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curves of modified A 302 grade B of the present project to the test material used by MEA.

J-R curve data are presented in the form of J_{Ic} values, J at 0.1 in (2.5 mm) of crack growth, and T-modulus. The tabulated values were averaged over all specimen sizes. Among the three orientations of transverse (T-L), longitudinal (L-T), and short transverse (L-S), the transverse almost always showed the lowest toughness (see Table 6.1). The general pattern for longitudinal and short transverse directions is that both were of higher toughness than transverse and that they tended to be about equal to each other. Representative J-R curves for each orientation of each material are presented in the report, and all were curve-fitted for reproduction purposes. The general pattern of test temperature effects was similarly characterized from averaged data, and representative J-R curves were also fitted for presentation (see Table 6.2). The fact that all heats showed J-R curve toughness loss with a test temperature increase to 550°F (288°C) is typical.

Drop-weight NDT tests were fairly consistent among heats, ranging from -30 to -5°F (-34 to -20°C). Evaluations for RT_{NDT} showed that two heats, one modified A 302 grade B and the vintage A 533 grade B, had insufficient CVN toughness at $NDT + 60°F$ (33°C) for RT_{NDT} to equal the NDT temperature (see Table 6.3). The penalties in RT_{NDT} were 43 and 44°F (24°C), respectively.

The multiple number of tests on one grade of steel provided an opportunity to evaluate J-R curve and J_{Ic} test methods that have been developed and modified within the ASTM. These methods have never been subjected to such a wide range of material characteristics and test conditions. The bottom-line finding from this experiment on the J_{Ic} method revisions made over a decade of development has the following two aspects: (1) the establishment of test practice requirements to obtain crack growth by compliance was found to be useful; (2) on the other hand, the revisions to test record analysis subsequent to the 1981 version of E 813 have made the determination of J_{Ic} unnecessarily

Table 6.1 Specimen orientation effects on J-R curve fracture toughness at 180°F
(averaged values over all specimen sizes; all specimens 20% side grooved)

Plate code	Orientation	CVN upper-shelf energy (ft-lb)	T slope	$J_{0.1}^a$ (in.-lb/in. ²)	J_{Ic}^a (in.-lb/in. ²)	Fitting curves	$J = A \cdot \Delta a^B \cdot \exp(C/\Delta a^{1/2})$		
							A	B	C
Z1, Z2	T-L	126	120	3300	1350	Z2B2-1T	7.110	0.287	-0.0596
	L-T	160	130	3810	1360	Z2B16-1T	9.513	0.300	-0.0761
	L-S	171	130	3480	1570	Z2B2-1/2T	8.715	0.259	-0.0846
Z3	T-L	85	90	2730	1200	Z310-1/2T	6.030	0.275	-0.0699
	L-T	129							
	L-S	127	70	2000	870	Z34-1/2T	5.633	0.435	-0.0014
Z4	T-L	111	100	3060	1420	Z49-1/2T	6.517	0.226	-0.0368
	L-S	130	130	4080	1880	Z47-2T	13.333	0.446	-0.0246
Z5	T-L	95	45	1630	1075	Z55-2T	3.043	0.177	-0.0329
	L-T	153	90	2640	1225	Z51-1T	6.178	0.318	-0.0348
	L-S	171	160	3895	1195	Z57-1/2T	15.718	0.567	-0.0199
Z6A	T-L	113	75	2325	1040	Z6A8-1T	6.377	0.410	-0.0131
	L-T	129	115	3570	1570	Z6A1-1T	12.299	0.518	-0.0114
	L-S	130	120	3550	1620	Z6A3-1/2T	8.542	0.267	-0.0752
Z6B	T-L	64	45	1470	810	Z6B9-1/2T	2.407	0.100	-0.0790
	L-T	117	70	2360	1130	Z6B1-1T	5.193	0.341	-0.0140
	L-S	114	85	2730	1183	Z6B8-1/2T	1.340	0.079	-0.0235
Z7	T-L	96	95	3000	1250	Z711-1T	8.742	0.446	-0.0279
	L-T	126	140	4500	2120	Z78-1T	12.057	0.383	-0.0501
	L-S	120	145	4405	2045	Z71-1/2T	10.637	0.326	-0.0780
Z8	T-L	96	85	2715	1245	Z83-1/2T	4.963	0.218	-0.0514
	L-T	134							
	L-S	154	145	4190	1860	Z816-1/2T	12.594	0.378	-0.0566

^aTo obtain J in units of kPa · m, multiply by 0.175.

Table 6.2 Test temperature effect on J-R curve (averaged values over all specimen sizes;
T-L orientation; all specimens 20% side grooved)

Plate code	Temperature (°F)	T slope	$J_{0.1}^a$ (in.-lb/in. ²)	J_{Ic}^a (in.-lb/in. ²)	Fitting curves	$J = A \cdot \Delta a^B \cdot \exp(C/\Delta a^{1/2})$		
						A	B	C
Z1, Z2	180	120	3300	1350	Z2B2-1T ^b	7.110	0.287	-0.0596
	400	70	2310	1100	Z2B11-1/2T	6.022	0.362	-0.0223
	550	60	2056	885	Z2B07-1T	5.619	0.421	-0.0095
Z3	180	90	2730	1200	Z310-1/2T ^b	6.030	0.275	-0.0699
	400	80	2130	830	Z311-1/2T	5.713	0.362	-0.0329
	550	45	1635	830	Z39-1/2T	3.394	0.282	-0.0268
Z4	180	100	3060	1420	Z49-1/2T ^b	6.517	0.226	-0.0368
	400	80	1995	1020	Z416-1/2T	4.858	0.297	-0.0318
	550	60	1950	975	Z42-2T	3.252	0.204	0.0311
Z5	180	45	1630	1075	Z55-2T ^b	3.043	0.177	-0.0329
	400	40	1475	895	Z514-1/2T	2.335	0.136	-0.0600
	550	35	1135	585	Z512-1/2T	2.267	0.233	-0.0188
Z6A	180	75	2325	1040	Z6A8-1T ^b	6.377	0.410	-0.0131
	400	55	1685	805	Z6A16-1/2T	4.490	0.343	-0.0216
	550	50	1595	825	Z6A15-1/2T	3.383	0.303	-0.0201
Z6B	180	45	1470	810	Z6B9-1/2T ^b	2.407	0.100	-0.0790
	400	32	1175	665	Z6B11-1/2T	1.801	0.150	-0.0352
	550	24	885	530	Z6B7-1T	1.301	0.146	-0.0113
Z7	180	95	2995	1255	Z711-1T ^b	8.742	0.446	-0.0279
	400	60	1995	995	Z714-1T	4.220	0.340	-0.0151
	550	50	1855	790	Z715-1T	3.487	0.281	-0.0140
Z8	180	85	2715	1250	Z83-1/2T ^b	4.963	0.218	-0.0514
	400	65	2095	1070				
	550	50	1685	860	Z88-1/2T	3.889	0.393	0.0124

^aTo obtain J in units of kPa · m, multiply by 0.175.

^bCurve can be found in Appendix A of NUREG/CR-6246.

Table 6.3. Reference temperature determinations

Plate code	NDT temperature [°F (°C)]	Charpy properties at NDT + 60°F, T-L orientation		
		Energy [ft-lb (J)]	Lateral expansion [in. (mm)]	RT _{NDT} [°F (°C)]
Z1, Z2	-5 (-20)	93 (127)	0.063 (1.6)	-5 (-20)
Z3	-15 (-26)	60 (82)	0.046 (1.2)	-15 (-26)
Z4	-15 (-26)	64 (88)	0.058 (1.5)	-15 (-26)
Z5	-30 (-34)	56 (77)	0.048 (1.2)	-30 (-34)
Z6A	-30 (-34)	65 (89)	0.052 (1.3)	-30 (-34)
Z6B	-30 (-34)	38 (52)	0.034 (0.9)	13 ^a (-10)
Z7	-15 (-26)	64 (88)	0.056 (1.4)	-15 (-26)
Z8	-20 (-29)	33 (45)	0.038 (1.0)	24 ^a (-4)

^aDetermined from CVN 50-ft-lb (lower-bound) temperature -60°F.

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complex and hence less useful. The revisions showed no beneficial impact on the precision of J_{IC} determination for modified A 302 grade B steels.

Two parameters of material crack-growth resistance measurement that were useful for comparisons were T-modulus and J at 0.1 in (2.5 mm) of crack growth. Each required a definition specific to this report, because neither parameter is covered by an ASTM standard analysis procedure.

6.2 Dynamic Fracture Toughness

(R. K. Nanstad, D. E. McCabe, and R. E. Link)

A section of HSST plate 14 was shipped to CDNSWC, Annapolis, Maryland, for use in meeting the subcontract provisions according to the HSST specification, "Dynamic Fracture Initiation Fracture-Toughness Testing of a Reactor Pressure Vessel Steel." Preliminary material characterization experiments were completed to allow for test temperature selection for conduct of the fracture-toughness tests. On June 1, 1995, a readiness review of the program was conducted at the CDNSWC facilities; members of the

Readiness Review Team (RRT) were Prof. Stan Rolfe (University of Kansas), J. G. Merkle, and D. E. McCabe. The RRT report provided a positive review of the CDNSWC preparations and presented four specific recommendations for action prior to conduct of the planned test program; these recommendations were transmitted to CDNSWC on June 27, 1995, with authorization to execute the test program as soon as the high-strain-rate valve on the test machine was made operational.

By the end of this reporting period, virtually all of the material characterization (Charpy impact, drop-weight NDT, tensile, and quasi-static fracture toughness) testing had been completed. Additionally, test fixtures were designed and manufactured; transducers and instrumentation were obtained, optimized, and set up; and the high-rate system was tuned for dynamic testing. The high-rate system had also been repaired, reinstalled, and tested in preparation for conduct of the actual dynamic fracture-toughness tests. A total of 6 high-rate tension and 18 high-rate fracture-toughness tests will be conducted as part of this program; both 2TC(T) and 4TC(T) specimens will be tested at loading rates up to $10^5 \text{ MPa}\sqrt{\text{m}}/\text{s}$. It is anticipated that the testing program will be completed by the end of February 1996.

7 Integration of Results

J. G. Merkle

7.1 Consideration of Prior Ductile Tearing in Estimating K_{Ic} from Small-Specimen Fracture-Toughness Data in the Transition Regime

7.1.1 Introduction

The development of procedures for experimentally measuring the fracture toughness of structural metals reached its first milestone with the publication of ASTM Standard E399,¹ first published in 1968.² This standard prescribes specimen size requirements (expressed in terms of the fracture toughness to be measured and the yield stress) sufficient for fracture to occur in the linear elastic range of the load-displacement curve and for the measured fracture-toughness value to be effectively independent of further increases in specimen dimensions. While testing under the conditions prescribed by E399 is practical for high-strength steels, such is not the case for lower-strength steels, including pressure vessel steels. The required specimen sizes for pressure vessel steels are unreasonably large. For application of fracture mechanics to pressure vessel steels that involve thick sections and conditions of potentially high triaxial constraint, values of plane-strain fracture toughness, K_{Ic} , are still needed, but the specimens available for testing are too small to allow direct measurement of those values. The measured values of toughness will generally exceed K_{Ic} . Therefore the value of K_{Ic} being sought must be determined indirectly. The provisions of E399 made use of the empirical relationship between measured values of fracture toughness and increasing specimen size only to the extent of avoiding the limited range of sizes within which fracture toughness tends to decrease with increasing specimen size. The use of small specimens for indirectly determining plane-strain fracture toughness makes it necessary to understand and use exactly that portion of the fracture toughness vs specimen size relationship that is avoided by E399, to relate the measured values of fracture toughness to the value of K_{Ic} being sought.

Two fundamental problems are involved in trying to quantitatively interpret variations of fracture toughness with specimen size in order to estimate K_{Ic} from generally elevated values of fracture toughness measured with small specimens. The first problem is to perceive the true pattern of variation of measured fracture toughness as a function of several interacting variables, including specimen dimensions and their ratios, yield stress, temperature, and the

presence or absence of stable ductile crack growth before cleavage. The second problem is to determine the physical causes of the observed variations of fracture toughness in order to construct rational mathematical relationships for use in estimating K_{Ic} . The problems of perceiving patterns and determining causes cannot easily be approached separately, and historically they have often been approached together, frequently on the basis of a hypothesis concerning physical cause and effect.

Two hypotheses have arisen concerning the physical cause of the elevation of average fracture-toughness values with decreasing specimen size. The first hypothesis is that, as the crack-tip plastic zone grows under rising load and slip lines reach free surfaces, stress triaxiality near the crack tip is relaxed, causing ductility and hence fracture toughness to increase. This hypothesis is known as the physical-loss-of-constraint hypothesis. The second hypothesis is that the small discrete metallurgical features that cause the initiation of cleavage microcracks are randomly distributed throughout a steel, causing every unit volume along a crack front to have a certain finite probability of failure at a given applied value of J or K_I . This hypothesis leads to a failure probability that increases with increasing crack-front length, and it is therefore known as the statistical (crack length) size-effect hypothesis. These hypotheses are not necessarily mutually exclusive.

7.1.2 Examinations of Data

One of the earliest experiments designed to reveal and illustrate the physical effects of loss of triaxial constraint was a series of impact energy tests conducted on laminated CVN specimens of mild steel.^{3,4} As illustrated in Fig. 7.1, the specimens were constructed of laminated layers oriented either parallel to the top and bottom surfaces (crack-arrestor laminates) or parallel to the side surfaces (crack-divider laminates). The laminating material was a ductile silver solder capable of developing only a relatively low tensile stress. The intended effect of the laminated joints was to limit the buildup of either the longitudinal or the transverse tensile stresses near the notch tip, and there was no change in the notch-front length. In the case of both layer orientations, as shown in Fig. 7.1, the effect of the soldered joints was to increase the impact energy values for the specimens tested in the transition range of temperature. In the case of the crack-divider laminates, the thinner and more numerous the layers, the higher was the impact energy in the transition range. These tests clearly

Figure 7.1 Impact energy data for solder-laminated CVN specimens demonstrating increased toughness due to reduction of triaxial constraint caused by soft silver solder (Source: Ref. 4)

demonstrated that, for fixed specimen dimensions, loss of triaxial constraint increases fracture resistance in the transition range.

Considering the often observed random variability of fracture-toughness values for nominally identical steel specimens in the transition range and the apparent decrease in the mean value of fracture toughness with increasing specimen size, Landes and Shaffer⁵ proposed to represent these variations statistically, in terms of a Weibull cumulative probability function. This proposal is the origin of the statistical (crack length) size-effect hypothesis. One set of data analyzed by Landes and Shaffer⁵ was for twenty-six 1T CT and five 4T CT specimens of ASTM A471 rotor steel tested at five different temperatures. This set of data, shown plotted in Fig. 7.2, displays what appears to be a less consistent size effect at the highest two test temperatures than at the lower test temperatures. This behavior could be tentatively interpreted as a decrease in specimen size effects with increasing test temperature, in the mid-to-upper transition range. However, there were not nearly enough 4T CT specimens tested to draw anything but an inference on this matter. Furthermore, the authors chose to attribute the relatively high value of fracture toughness measured with one 4T CT specimen at 52°C to randomness in the occurrence, or lack of occurrence, of low-toughness cleavage-initiation sites.

Additional early observations on the interacting effects of specimen size and fracture mode were made by Dawes* at the Welding Institute. As summarized schematically in Fig. 7.3, specimen-size effects on purely cleavage-fracture toughness develop as temperature increases above the highest temperature at which LEFM validity requirements can be met for the smallest specimens being considered. However, with further increases in temperature, the occurrence of ductile tearing by microvoid coalescence seems to be accompanied by a decrease, and eventual elimination, of size effects. As indicated in Fig. 7.3, as temperature increases, ductile tearing prior to cleavage seems to cause the curve of toughness (presumably measured at maximum load) vs temperature for the smaller specimen size to approach and eventually cross the curve for the larger specimen size, near the beginning of the ductile upper-shelf temperature range. Observations of the same nature were made by Milne and Curry,⁶⁻⁸ using fracture-toughness data obtained from notched-bend specimens of BS 1501-271 steel. As shown in Fig. 7.4, below 0°C fracture was exclusively by cleavage, and between -70°C

*M. G. Dawes, "The Significance of Different Micromechanisms of Crack Extension and Fracture Behaviors to Elastic and Elastic-Plastic Fracture Characterizing Parameters," presented at the ASTM Subcommittee E24.08 Meeting, Bal Harbor, Florida, Nov. 13, 1980.

Figure 7.2 Toughness data for ASTM A471 rotor steel demonstrating possible decrease in specimen-size effects in mid-to-upper transition regime (Source: Ref. 5. Permission to use this copyrighted material is granted by the American Society for Testing and Materials.)

Figure 7.3 Schematic diagram illustrating combined effects of specimen size and ductile tearing before cleavage on relative toughnesses measured with small and large specimens (Source: M. G. Dawes, "The Significance of Different Micromechanisms of Crack Extension and Fracture Behaviors to Elastic and Elastic-Plastic Fracture Characterizing Parameters," presented at the ASTM Subcommittee E24.08 Meeting, Bal Harbor, Florida, Nov. 13, 1980. Permission to use this uncopyrighted material is granted by The Welding Institute.)

Figure 7.4 Illustration of convergence of small- and large-specimen fracture-toughness curves in upper transition region, caused by constraint recovery due to ductile crack growth prior to cleavage (Source: Ref. 8. Permission to use this copyrighted material is granted by the American Society for Testing and Materials.)

and 0°C small specimens produced a higher curve of mean K_{IJ} vs temperature than did larger specimens. However, at higher temperatures, at which ductile crack growth preceded fracture by cleavage, the size effect disappeared. Milne and Curry⁶ concluded from this observation that ductile crack growth had caused a change in the shape and sharpness of the crack tip which, combined with other effects, had increased stress triaxiality local to the growing crack tip.

7.1.3 Trial Applications of Existing Size-Effect Adjustments

The initial development of methods for representing the physical and statistical effects of specimen size on fracture toughness in the transition range began before or concurrently with the observations concerning the effects of ductile crack growth before cleavage just described.

Partly for this reason and partly for simplicity, the first attempts to adjust small-specimen fracture-toughness data to estimate K_{IC} did not consider the effects of ductile crack growth preceding cleavage.

The first method for estimating K_{IC} from elevated small-specimen fracture-toughness data was developed by Irwin,⁹ based on the premise that through-thickness constraint becomes relaxed as a function of the ratio of formally calculated plastic zone size to specimen thickness. The empirical relationship from which K_{IC} is calculated is the cubic equation¹⁰

$$\beta_c = \beta_{IC} + 1.4\beta_{IC}^3, \quad (7.1)$$

in which

$$\beta_c = \frac{\left(\frac{K_c}{\sigma_Y}\right)^2}{B}, \quad (7.2)$$

and

$$\beta_{Ic} = \frac{\left(\frac{K_{Ic}}{\sigma_Y}\right)^2}{B}. \quad (7.3)$$

K_c and K_{Ic} are the measured and plane-strain fracture-toughness values, respectively; σ_Y is the yield stress; B is the specimen thickness; and the constant 1.4 is an empirically chosen value. The rationale for the form of Eq. (7.1) and the procedure for its solution to obtain K_{Ic} are discussed in detail in Ref. 10. An early successful application of Eq. (7.1) to the analysis of a set of fracture-toughness data for ASTM A-372 Class 4 steel was made by Wessel¹¹ in 1964. Other applications have been to the analysis of large surface-cracked tensile bars of A 533 B steel;¹² cracks in the inside nozzle-corner regions of experimental pressure vessels, the nozzles of which were constructed of A 508 Class 2 forging steel;¹³ and shallow-cracked notched-bend specimens of A 533 B steel.¹⁴ In the latter three applications, the distance governing constraint was taken to be the crack depth instead of the specimen half-thickness, so that B was replaced by $2a$ in Eqs. (7.2) and

(7.3). An additional study of the results obtained from applying Eq. (7.1) to several sets of small-specimen fracture-toughness data was reported in Ref. 10. The pattern that emerged is well illustrated by Fig. 7.5, in which the open round points represent fracture-toughness values for the forged and heat-treated cylinder used for the seventh HSST Program thermal-shock experiment (TSE-7), measured with 1T CT specimens, and the closed triangles represent K_{Ic} values calculated directly from the thermal-shock test data.^{15,16} Below RT_{NDT} , which for the TSE-7 material^{15,16} was -1°C , the adjusted toughness values are near the lower bound of the 1T CT data, close to the TSE-7 data, and above the ASME lower-bound K_{Ic} curve. However, above RT_{NDT} , the adjusted data are too low, lying below the K_{Ic} values measured in TSE-7 and eventually below the ASME lower-bound K_{Ic} curve. It can also be seen from Fig. 7.5 that at -100°C there is very little adjustment and that the lower bound of the 1T CT data is very close to the ASME K_{Ic} curve. As temperature increases, the lower bound of the 1T CT data first rises above the ASME lower-bound K_{Ic} curve, but then, for $(T - RT_{NDT}) > 0$, it approaches the K_{Ic} curve again, meeting it at about $(T - RT_{NDT}) = 50^\circ\text{C}$. In Ref. 10, it was also observed that, for two steels tested by Iwadata et al.,¹⁷ significant ductile crack growth before cleavage began to occur at about RT_{NDT} . Assuming this to be a general trend leads to the hypothesis that the overadjustment of small-specimen fracture-toughness data above RT_{NDT} displayed in Fig. 7.5 results from the neglect of the effects of ductile crack growth before cleavage in the method of adjustment.

Figure 7.5 Results of applying the Irwin β_{Ic} specimen-size adjustment to 1T CT characterization data for the cylinder of seventh HSST Program thermal-shock experiment (TSE-7)

Integration

The statistical (crack length) size-effect hypothesis has also been examined for several of the same sets of data examined in Ref. 10, with typical results¹⁸ also obtained for the cylinder of TSE-7. The reasoning behind this adjustment is as follows. The three-parameter Weibull equation for cumulative failure probability is¹⁸

$$P_f = 1 - \exp \left\{ - \frac{B}{B_0} \left(\frac{K_I - K_{\min}}{K_0 - K_{\min}} \right)^4 \right\}, \quad (7.4)$$

where P_f is cumulative failure probability for a toughness equal to or less than K_I , B is crack length (which is the same as specimen thickness for a through-cracked specimen), $K_{\min}$ is a mathematical fitting constant empirically set equal to $20 \text{ MPa}\sqrt{\text{m}}$, and B_0 and K_0 are normalization constants. For two specimens, one a 1T CT material-characterization specimen and the other a thermal-shock cylinder with an axially oriented inside-surface part-through crack of length $2c$, the toughness values corresponding to the same value of P_f are related by the expression

$$K_{I(2c)} = \left[K_{I(1T)} - K_{\min} \right] \left(\frac{B_{1T}}{2c} \right)^{1/4} + K_{\min}, \quad (7.5)$$

which is obtained directly from Eq. (7.4). Thus Eq. (7.5) can be used to adjust the toughness values measured with 1T CT specimens to values that would be expected to

govern the extension of a crack of length $2c$ in a thermal-shock cylinder. Although the initial flaw for TSE-7 was a semielliptical surface flaw with a depth of 14 mm and length of 38 mm, its first extension rendered its surface length almost equal to that of the cylinder. Thus in Ref. 18 the flaw in TSE-7 was considered to be effectively 1000 mm long. Applying Eq. (7.5) to the 1T CT characterization data for the cylinder material of TSE-7 produces the ranges of adjusted data indicated by the shaded vertical bars shown in Fig. 7.6. Also shown in Fig. 7.6 are the ranges of β_{IC} -adjusted data from Fig. 7.5. The results of the Weibull adjustment are not much different from those of the β_{IC} adjustment, except that the downward adjustment is greater at low temperatures, with some values falling below the ASME K_{IC} curve, and the range of adjusted values becomes greater than for the β_{IC} adjustment as temperature increases. The Weibull procedure also overadjusts data in the upper transition range, as does the β_{IC} procedure. Thus not only does it appear that size effects on fracture toughness do diminish with increasing temperature above RT_{NDT} , but also that methods of adjusting small-specimen fracture-toughness data to estimate K_{IC} that do not consider this phenomenon become overconservative above RT_{NDT} for this reason.

The above observations, based mainly on the behavior of data from 1T CT specimens, appear initially somewhat at odds with earlier successful experience with applications of the β_{IC} adjustment to estimating high levels of toughness

Figure 7.6 Comparison of results from applying Weibull and β_{IC} specimen-size adjustments to 1T CT characterization data for TSE-7 material

in the upper transition regime for surface-cracked tensile bars¹² and nozzle-corner cracks.¹³ However, it is possible to reconcile these observations by recognizing the role of ductile crack growth before cleavage and the fact that compact specimens and notched beams remain relatively constrained after net section yielding, thereby producing lower J-R curves than do geometries containing surface cracks under effectively uniaxial tension. With these considerations in mind, it is logical to hypothesize greater, and more pronounced effects of, ductile crack growth before cleavage at given toughness levels for compact specimens and notched beams than for surface-cracked geometries lacking transverse restraint.

Recent computational evidence strengthens the hypothesis that ductile crack growth promotes a recovery of constraint. Varias and Shih¹⁹ performed a modified-boundary-layer, plain-strain, small-strain, elastic-plastic, finite-element analysis, with equations appropriate for steady-state rate and inertia-independent crack growth. Their results showed that a high-constraint, single-parameter, stress field always tends to develop during ductile crack growth, regardless of the level of constraint before crack growth. Tang and Dodds²⁰ confirmed the results of Ref. 19 by analyzing the early transient phase of crack growth in plane strain. They found a progressive increase of the normal stresses in the region surrounding the crack tip, toward values generally slightly higher than the small-scale-yielding values. In the yielded region behind the growing crack, the horizontal stress was especially increased, apparently because of the resistance to the plastic contraction required to accommodate crack opening. This stress increase could be responsible for the elevation of the hydrostatic stress ahead of the crack tip and the observed reduced opening (so-called crack sharpening) characteristic of growing cracks.

The consistency of these computational results with the experimental and empirical evidence described earlier is encouraging. Considering all the evidence now available, it appears desirable to develop a method for adjusting small-specimen fracture-toughness data to estimate K_{Ic} that considers the effects of ductile tearing before cleavage, when it occurs.

7.1.4 Development of a Modified Analysis

Examination of several additional sets of small-specimen fracture-toughness characterization data, including those for the cylinders of HSST Program thermal-shock experiments TSE-5, 5A, and 6, as plotted in Ref. 10, plus the data for HSSI welds 72W and 73W in both the unirradiated and irradiated conditions,²¹ shows a pattern basically consistent

with that displayed by the data for the cylinder of TSE-7 plotted in Fig. 7.5. The effect of specimen size begins to diminish as temperature increases above RT_{NDT} , and no consistent size effects are in evidence for $(T - RT_{NDT}) \geq 50^\circ\text{C}$. There are materials for which the value of RT_{NDT} is uncertain or unusually high with respect to measured values of fracture toughness for static initiation, and these materials may not fit the same pattern. Nevertheless, the development of a size-effect adjustment procedure consistent with the observed pattern seems worthwhile. Because the β_{Ic} adjustment based on Eq. (7.1), unlike the Weibull adjustment based on Eq. (7.4), produces estimates of asymptotic toughness, K_{Ic} , a temperature-dependent value of the coefficient c in Eq. (7.1) should be a practical way of dealing with the observed decrease of size effects with increasing temperature.

Equation (7.1) is solved for K_{Ic} by first calculating β_c from Eq. (7.2), using K_{Jc} to represent K_c . Then a quantity denoted by m is calculated from

$$m = \frac{\beta_c}{2c} \quad (7.6)$$

and two quantities, denoted A_1 and A_2 , are calculated from

$$A_1 = \sqrt{m^2 + \left(\frac{1}{3c}\right)^3} + m \quad (7.7)$$

and

$$A_2 = \sqrt{m^2 + \left(\frac{1}{3c}\right)^3} - m \quad (7.8)$$

The value of β_{Ic} is calculated from

$$\beta_{Ic} = A_1^{1/3} - A_2^{1/3} \quad (7.9)$$

and K_{Ic} is calculated from

$$K_{Ic} = K_{Jc} \sqrt{\frac{\beta_{Ic}}{\beta_c}} \quad (7.10)$$

The simplest method for making the coefficient c a function of temperature, but only for $0 \leq (T - RT_{NDT}) \leq 50^\circ\text{C}$, is to adopt the following scheme.

For $(T - RT_{NDT}) \leq 0$,

$$c = 1.4 \quad (7.11)$$

Integration

For $0 \leq (T - RT_{NDT}) < 50^\circ\text{C}$,

$$c = 1.4 \left[1 - \frac{(T - RT_{NDT})}{50} \right] \quad (7.12)$$

For $(T - RT_{NDT}) \geq 50^\circ\text{C}$,

$$c = 10^{-8} \quad (7.13)$$

The small value of c prescribed by Eq. (7.13) eliminates problems in Eqs. (7.6) through (7.8) that would otherwise occur because of attempted division by zero. The result is numerically the same as ignoring the adjustment altogether, which is the real objective, for $(T - RT_{NDT}) \geq 50^\circ\text{C}$, because, for $c = 0$, Eq. (7.1) reduces to $\beta_{IC} = \beta_C$.

For the TSE-7 characterization data shown in Figs. 7.5 and 7.6, the results of applying the modified β_{IC} procedure are as shown in Fig. 7.7. The overcorrection evident in Figs. 7.5 and 7.6 has been eliminated, and the adjusted values are consistent with the values of K_{IC} measured in TSE-7. At the highest test temperature, the large range of values, which were not subject to adjustment, is consistent with the very steep slope of the toughness-vs-temperature curve at that temperature. Preliminary results for other materials are

similar, although only a few data sets cover as broad a range of temperature as do the data for the cylinder of TSE-7. Thus the results of the modified β_{IC} adjustment seem reasonable. They are also consistent with the premise that the ASME lower-bound K_{IC} curve is a realistic low-tolerance bound estimate of K_{IC} . Additional evaluations are in progress.

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CONVERSION FACTORS^a

SI unit	English unit	Factor
mm	in.	0.0393701
cm	in.	0.393701
m	ft	3.28084
m/s	ft/s	3.28084
kN	lbf	224.809
kPa	psi	0.145038
MPa	ksi	0.145038
MPa $\cdot\sqrt{m}$	ksi $\cdot\sqrt{in.}$	0.910048
J	ft $\cdot$ lb	0.737562
K	$^{\circ}F$ or $^{\circ}R$	1.8
kJ/m ²	in. $\cdot$ lb/in. ²	5.71015
W $\cdot$ m ⁻² $\cdot$ K ⁻¹	Btu/h $\cdot$ ft ² $\cdot$ $^{\circ}F$	0.176110
kg	lb	2.20462
kg/m ³	lb/in. ³	3.61273×10^{-5}
mm/N	in./lbf	0.175127
$T(^{\circ}F) = 1.8(^{\circ}C) + 32$		

^aMultiply SI quantity by given factor to obtain English quantity.

Prior Heavy-Section Steel Technology Program Publications

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11. ABSTRACT (200 words or less)

The Heavy-Section Steel Technology (HSST) Program is conducted for the Nuclear Regulatory Commission (NRC) by Oak Ridge National Laboratory (ORNL). The program's focus is on the development and validation of technology for the assessment of fracture-prevention margins in commercial nuclear reactor vessels. The HSST program is organized in seven tasks: (1) program management, (2) constraint effects analytical development and validation, (3) evaluation of cladding effects, (4) ductile-to-cleavage fracture-mode conversion, (5) fracture analysis methods development and application, (6) material property data and test methods, and (7) integration of results. The program tasks have been structured to place emphasis on resolution of fracture mechanics issues with near-term licensing significance. Resources to execute the research tasks are drawn from ORNL with sub-contract support from universities, and other research laboratories. Close contact is maintained with the sister Heavy-Section Steel Irradiation (HSSI) Program at ORNL and with related research programs both in the United States and abroad. This report provides an overview of principal developments in each of the seven program tasks from April 1995 through September 1995.

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