Heavy-Section Steel Technology Program

Semiannual Progress Report
for April-September 1996

Manuscript Completed: March 1998
Date Published: August 1998

Prepared by
W. E. Pennell

Oak Ridge National Laboratory
Managed by Lockheed Martin Energy Research Corp.

Oak Ridge National Laboratory
Oak Ridge, TN 37831-6285

S. N. M. Malik, NRC Project Manager

Prepared for
Division of Engineering Technology
Office of Nuclear Regulatory Research
U.S. Nuclear Regulatory Commission
Washington, DC 20555-0001
NRC Job Code B0119

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED *ph*

MASTER

**NUREG/CR-4219, Vol. 13, No. 2 has been
reproduced from the best copy available.**

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

Abstract

The Heavy-Section Steel Technology (HSST) Program is conducted for the U.S. Nuclear Regulatory Commission by Oak Ridge National Laboratory (ORNL). The focus of the program is on the development, validation, and application 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 emphasize the resolution of fracture issues with near-term licensing significance. Resources to execute the research tasks are drawn from ORNL, and subcontract support comes 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 1996 to September 1996.

Contents

	Page
Abstract	iii
List of Figures	vii
List of Tables	xi
Preface	xiii
Executive Summary	xv
1 Program Management	1
1.1 Objectives	1
1.2 Organization and Staffing	1
1.3 Planning and Program Direction	1
1.4 Subcontracts and Materials Procurement	2
1.5 International Interactions	3
1.6 Codes and Standards	4
1.7 Reports, Papers, and Presentations	5
References	5
2 Constraint Effects Analytical Development and Validation	7
2.1 Biaxial-Loading Effects on Constraint	7
2.1.1 Experimental Developments in Biaxial Test Program	7
2.1.1.1 Installation of New Large-Scale Biaxial Test Fixture	7
2.1.1.2 Specimen Fabrication	7
2.1.2 Cruciform Testing and Results	8
2.2 Biaxial-Loading Effects on Constraint	16
2.2.1 Plate 14 Cruciform Results	16
2.2.2 Analytical Investigation of Constraint Effects	17
References	22
3 Evaluation of Cladding Effects	23
3.1 Introduction	23
3.2 Clad Cruciform Beam Testing Program	23
3.2.1 Cruciform Beam Specimen	23
3.2.2 Specimen Validation	27
3.2.3 Testing of Cruciform Clad Specimens	27
3.2.4 Analysis of Cruciform Clad Beams	27
3.2.5 Biaxial Loading and Cleavage Fracture Toughness	31

3.3	Role of Residual Stresses in Clad Finite-Length Flaw	34
3.4	Material Characterization of RPV Shell Material	40
3.4.1	Drop-Weight Specimens	40
3.4.2	Tensile Specimens	40
3.5	Large-Scale Biaxial Test Fixture	40
3.6	Analysis of Full-Thickness Clad Beams	40
3.7	Development of Clad-Yielding Model	40
	References	46
4	Ductile-to-Cleavage Fracture-Mode Conversion	49
4.1	Introduction	49
4.2	Fracture-Mode Conversion Model Development	49
4.2.1	Computer Program Development	49
4.2.2	Calibration of Simplified Gurson Model	51
	References	55
5	Fracture Analysis Methods Development and Applications	57
5.1	Development of Stress-Intensity-Factor Influence Coefficients	57
5.2	PTS Applications	57
5.2.1	Example Problem Definition	58
5.2.2	Flaw Distributions	59
5.2.3	Analysis Results	62
5.3	NRC Support	64
	References	64
6	Material Property Data and Test Methods	67
6.1	J-R Curve Evaluations on A 302 Grade B Steel	67
6.2	Dynamic Fracture Toughness	67
	References	67
7	Integration of Results	69
7.1	Relation Between Observed Locations of Cleavage Trigger Points and the Constraint Parameter M	69
7.1.1	Background	69
7.1.2	Application of Fractographic Data	70
	References	74
	Conversion Factors	75
	Prior Heavy-Section Steel Technology Program Publications	77

List of Figures

Figure		Page
1.1	HSST Program Level 1 work breakdown structure	1
1.2	Resources applied to the HSST Program research and development tasks	2
2.1	Demonstration of nominal 0.6:1 biaxial-loading capability for new large-scale cruciform test fixture	8
2.2	CMOD for failure test of cruciform specimen P4B, $P_T/P_L = 1:1$, $T_f = 6^\circ\text{C}$ (42°F)	10
2.3	LLD for failure test of cruciform specimen P4B, $P_T/P_L = 1:1$, $T_f = 6^\circ\text{C}$ (42°F)	10
2.4	Fracture surface of cruciform specimen P4B showing location of two apparent initiation sites	11
2.5	CMOD for failure test of cruciform specimen P6A, $P_T/P_L = 0.6:1$, $T_f = 4^\circ\text{C}$ (40°F)	12
2.6	LLD for failure test of cruciform specimen P6A, $P_T/P_L = 0.6:1$, $T_f = 4^\circ\text{C}$ (40°F)	12
2.7	Fracture surface of cruciform specimen P6A. Location of apparent initiation site is near centerline of specimen	13
2.8	CMOD for failure test of cruciform specimen P10A, $P_T/P_L = 0.6:1$, $T_f = 8^\circ\text{C}$ (46°F)	14
2.9	LLD for failure test of cruciform specimen P10A, $P_T/P_L = 0.6:1$, $T_f = 8^\circ\text{C}$ (46°F)	14
2.10	Fracture surface of cruciform specimen P10A.....	15
2.11	Cruciform specimen data from heat treated plate 14	15
2.12	Plate 14 cruciform data showing effect of increasing flaw-tip constraint (biaxial loading) on fracture toughness in lower transition temperature range	16
2.13	$K_J - \ln(R_{pl})$ trajectories using proportional limit (equivalent plastic strain = 0%) to determine R_{pl}	17
2.14	$K_J - \ln(R_{pl})$ trajectories using conventional yield strain (equivalent plastic strain of 0.2%) to determine R_{pl}	18
2.15	$K_J - \ln(R_{pl})$ trajectories using equivalent plastic strain of 1% to determine R_{pl}	18
2.16	$K_J - \ln(R_{pl})$ trajectories using equivalent plastic strain of 5% to determine R_{pl}	19
2.17	$K_J - \ln(R_{pl})$ trajectories for a $102 \times 102 \times 102$ mm specimen with deep flaw ($a/W = 0.5$)	20
2.18	$K_J - \ln(R_{pl})$ trajectories for shallow- and deep-flaw cruciform specimen under uniaxial or biaxial bending	21
2.19	$K_J - \ln(R_{pl})$ trajectories for shallow-flaw cruciform specimen under uniaxial or biaxial bending	21
3.1	Cruciform shallow-flaw fracture toughness specimen approximates biaxial stresses produced by PTS loading in an RPV wall	25

3.2	Finite-length flaw specimens cut from an RPV shell segment are used to investigate the effects of the clad layer and flaw size on initiation behavior of prototypic RPV welds	25
3.3	Clad cruciform specimens are fabricated from either a machined test section or a uniaxial beam	26
3.4	Tests of clad cruciform beams with shallow, finite-length surface flaws are used to characterize the effects of biaxial loading ratio and material composition cleavage initiation behavior in RPV materials	28
3.5	Unique cleavage initiation site for clad cruciform beam tested under uniaxial loading	29
3.6	Summary of location of cleavage initiation sites for clad cruciform specimen tests	29
3.7	Finite-element model used to analyze cruciform beam specimens: (a) quarter section; (b) detail of flaw geometry	30
3.8	Schematic showing relationship between circular angle and location of cleavage initiation site on semielliptical flaw boundary	30
3.9	Calculated SIF vs angle along flaw front and applied moment loading for clad-beam specimen under uniaxial (0.0) loading	31
3.10	Calculated SIF vs angle along flaw front and applied moment loading for clad-beam specimen under biaxial (1.0) loading	32
3.11	Calculated SIF, K_J , vs angle along flaw front and applied moment loading for HAZ beam specimen under uniaxial (0.0) loading	32
3.12	Calculated SIF, K_J , vs angle along flaw front and applied moment loading for HAZ beam specimen under biaxial (1.0) loading	33
3.13	Comparison of clad and HAZ cruciform beam data with ASME Sect. XI K_{Ic} curve	33
3.14	Biaxial tests in (a) heat-treated A 533 B plate material with constant-depth, shallow surface flaws and (b) prototypic RPV weld material with finite-length semielliptical surface flaws show a consistent trend of decreasing fracture toughness with increasing load-biaxiality ratio	34
3.15	Schematic of inspection blocks used to measure clad layer distortion caused by relaxation of residual stresses	35
3.16	Finite-element model for clad residual stress investigation	36
3.17	Opening mode stress distributions in 3-step 2-D analysis to determine residual stress in clad layer for clad cruciform specimens	37
3.18	Residual stress distributions in 102-mm beam via (1) truncated temperature distribution and (2) element death	38
3.19	Weld residual stress distributions in 4- and 9-in.-deep beams via thermal gradient method (TGM)	38
3.20	Variation of K_J along crack front for finite-length flaw clad cruciform beam under uniaxial (0:1) loading	39

3.21	Variation of KJ along crack front for finite-length flaw clad cruciform beam under biaxial (1:1) loading	39
3.22	Three different specimen configurations used to characterize the tensile properties of the clad cruciform beam specimens	41
3.23	Stress-strain characterization of the component materials in the clad cruciform beam structure	42
3.24	Measurements of a prototypic RPV material show the HAZ area may have hardness numbers well above those for the clad layer and base material	43
3.25	Comparison of SIFs evaluated for the cracked beam in plane stress vs bending moment	45
3.26	Crack length for WF analyses were increased by a quantity proportional to the crack-tip plastic zone to achieve the same value as the finite element solution at 900 in.-kip	45
3.27	Comparison for plane-strain conditions	46
4.1	J- Δa curves predicted by plane-strain SE(B) analyses and average of three measured curves, $a/W = 0.6$	50
4.2	J- $\Delta \alpha$ curves predicted by plane-strain SE(B) analyses and average of three measured curves, $a/W = 0.14$	51
4.3	Small laboratory specimens used to calibrate Gurson model parameters f_0 and D	52
4.4	Three-dimensional finite-element model of 1T-CT specimen: (a) complete mesh and (b) crack-plane region	53
4.5	Comparison of calculated and measured LLD for specimens fabricated from SNUPPS longitudinal weld material	54
4.6	Comparison of calculated and measured crack extension for specimens fabricated from SNUPPS longitudinal weld material	54
5.1	Vessel beltline layout	58
5.2	Results of example regulatory PTS analyses	63
7.1	Nevalainen-Dodds results converted to notation of draft standard	69
7.2	Cumulative probability for 23 determinations of CTOD normalized trigger point distances	70
7.3	Figure 7.1 with inverted density function obtained from Weibull plot of Fig. 7.2	71
7.4	Figure 7.2 with filled data points indicating failure to meet $M = 30$ constraint limit	72
7.5	Figure 7.2 with filled data points indicating failure to meet $M = 50$ constraint limit	72
7.6	Principal stress at cleavage trigger point normalized by yield strength vs CTOD normalized trigger point distance	73
7.7	Weibull plot of all K_{Jc} data determined at -60°C [slope = $4K_{Jc(\text{med})} = 240 \text{ MPa}\sqrt{\text{m}}$]	73

List of Tables

Table		Page
2.1	Summary of heat-treated plate 14 cruciform specimen test results	9
3.1	Matrices of unclad and clad beam tests defined to validate constraint methodologies	24
3.2	Summary of cruciform clad beam test results	27
5.1	RPV beltline embrittlement map for illustrative PFM analysis	59
5.2	An example RR-PRODICAL output file	60
5.3	Flaw distributions by categories for welded regions	61
5.4	Flaw distributions used in illustrative problem for both methods of PTS analysis	61

Preface

The Heavy-Section Steel Technology (HSST) Program, which is sponsored by the U.S. 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 from April 1996 to September 1996. 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-1806 (ORNL/NUREG/TM-419)
ORNL-4315	NUREG/CR-1941 (ORNL/NUREG/TM-437)
ORNL-4377	NUREG/CR-2141, Vol. 1 (ORNL/TM-7822)
ORNL-4463	NUREG/CR-2141, Vol. 2 (ORNL/TM-7955)
ORNL-4512	NUREG/CR-2141, Vol. 3 (ORNL/TM-8145)
ORNL-4590	NUREG/CR-2141, Vol. 4 (ORNL/TM-8252)
ORNL-4653	NUREG/CR-2751, Vol. 1 (ORNL/TM-8369/V1)
ORNL-4681	NUREG/CR-2751, Vol. 2 (ORNL/TM-8369/V2)
ORNL-4764	NUREG/CR-2751, Vol. 3 (ORNL/TM-8369/V3)
ORNL-4816	NUREG/CR-2751, Vol. 4 (ORNL/TM-8369/V4)
ORNL-4855	NUREG/CR-3334, Vol. 1 (ORNL/TM-8787/V1)
ORNL-4918	NUREG/CR-3334, Vol. 2 (ORNL/TM-8787/V2)
ORNL-4971	NUREG/CR-3334, Vol. 3 (ORNL/TM-8787/V3)
ORNL/TM-4655 (Vol. II)	NUREG/CR-3744, Vol. 1 (ORNL/TM-9154/V1)
ORNL/TM-4729 (Vol. II)	NUREG/CR-3744, Vol. 2 (ORNL/TM-9154/V2)
ORNL/TM-4805 (Vol. II)	NUREG/CR-4219, Vol. 1 (ORNL/TM-9593/V1)
ORNL/TM-4914 (Vol. II)	NUREG/CR-4219, Vol. 2 (ORNL/TM-9593/V2)
ORNL/TM-5021 (Vol. II)	NUREG/CR-4219, Vol. 3, No. 1 (ORNL/TM-9593/V3&N1)
ORNL/TM-5170	NUREG/CR-4219, Vol. 3, No. 2 (ORNL/TM-9593/V3&N2)
ORNL/NUREG/TM-3	NUREG/CR-4219, Vol. 4, No. 1 (ORNL/TM-9593/V4&N1)
ORNL/NUREG/TM-28	NUREG/CR-4219, Vol. 4, No. 2 (ORNL/TM-9593/V4&N2)
ORNL/NUREG/TM-49	NUREG/CR-4219, Vol. 5, No. 1 (ORNL/TM-9593/V5&N1)
ORNL/NUREG/TM-64	NUREG/CR-4219, Vol. 5, No. 2 (ORNL/TM-9593/V5&N2)
ORNL/NUREG/TM-94	NUREG/CR-4219, Vol. 6, No. 1 (ORNL/TM-9593/V6&N1)
ORNL/NUREG/TM-120	NUREG/CR-4219, Vol. 6, No. 2 (ORNL/TM-9593/V6&N2)
ORNL/NUREG/TM-147	NUREG/CR-4219, Vol. 7, No. 1 (ORNL/TM-9593/V7&N1)
ORNL/NUREG/TM-166	NUREG/CR-4219, Vol. 7, No. 2 (ORNL/TM-9593/V7&N2)
ORNL/NUREG/TM-194	NUREG/CR-4219, Vol. 8, No. 1 (ORNL/TM-9593/V8&N1)
ORNL/NUREG/TM-209	NUREG/CR-4219, Vol. 8, No. 2 (ORNL/TM-9593/V8&N2)
ORNL/NUREG/TM-239	NUREG/CR-4219, Vol. 9, No. 1 (ORNL/TM-9593/V9&N1)
NUREG/CR-0476 (ORNL/NUREG/TM-275)	NUREG/CR-4219, Vol. 9, No. 2 (ORNL/TM-9593/V9&N2)
NUREG/CR-0656 (ORNL/NUREG/TM-298)	NUREG/CR-4219, Vol. 10, No. 1 (ORNL/TM-9593/V10&N1)
NUREG/CR-0818 (ORNL/NUREG/TM-324)	NUREG/CR-4219, Vol. 10, No. 2 (ORNL/TM-9593/V10&N2)
NUREG/CR-0980 (ORNL/NUREG/TM-347)	NUREG/CR-4219, Vol. 11, No. 1 (ORNL/TM-9593/V11&N1)
NUREG/CR-1197 (ORNL/NUREG/TM-370)	NUREG/CR-4219, Vol. 11, No. 2 (ORNL/TM-9593/V11&N2)
NUREG/CR-1305 (ORNL/NUREG/TM-380)	NUREG/CR-4219, Vol. 12, No. 1 (ORNL/TM-9593/V12&N1)
NUREG/CR-1477 (ORNL/NUREG/TM-393)	NUREG/CR-4219, Vol. 12, No. 2 (ORNL/TM-9593/V12&N2)
NUREG/CR-1627 (ORNL/NUREG/TM-401)	NUREG/CR-4219, Vol. 13, No. 1 (ORNL/TM-9593/V13&N1)

Executive Summary

W. E. Pennell

The Heavy-Section Steel Technology (HSST) Program is conducted for the U.S. Nuclear Regulatory Commission (NRC) by the Oak Ridge National Laboratory (ORNL). The focus of this program is on the development, validation, and application of technology to assess 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

A revised Statement of Work (SOW) was issued by NRC for preparing the final version of the HSST Program. The SOW added requirements for (1) material to be obtained from the beltline section of the RPV for the Pressure Vessel Research User's Facility (PVRUF) to use in the manufacture of future test specimens, (2) modification of the FAVOR computer program to perform the method 1 (best-estimate) and method 2 (probabilistic risk assessment) analyses, and (3) probabilistic fracture-mechanics analyses of two plants. The SOW also deleted the requirement for the generation of stress-intensity-factor (SIF) influence coefficients for RPVs with an R_i/t ratio of 15. Preparation of a 189, responsive to the revised SOW, was completed. A report on the core competency capabilities existing at ORNL in the area of reactor vessel integrity assessment was prepared and presented to the joint NRC-U.S. Department of Energy (DOE) committee investigating core nuclear capabilities at DOE national laboratories. The committee met at the NRC White Flint-2 offices April 16, 1996.

Research subcontracts were in progress with the University of Maryland (UM), the University of Illinois (UI), and the Carderock Division of the Naval Surface Warfare Center (CDNSWC). A consulting subcontract was in progress with Professor S. T. Rolfe of the University of Kansas. Discussions were held with UM personnel to complete the details of the crack-tip analysis to be performed in an investigation of the physical basis for the link between crack-tip-opening-displacement (CTOD) and plastic zone radius (R) under biaxial loading. All ORNL reviews were completed on a letter report by A. Gullerud and R. H. Dodds, Jr., of UI, entitled "Finite-Element Modeling of Ductile Crack Growth." Advanced copies of a report by R. E. Link of the U.S. Naval Academy and S. M. Graham of the Vector Research Company, Inc., entitled "Dynamic Fracture Initiation Toughness of ASTM A 533, Grade B Steel Plate," were received and distributed to HSST Program personnel for their review and comments.

Developments in the Heavy-Section Steel Irradiation (HSSI) Program plan for obtaining material from RPV welds necessitated the investigation of alternate approaches to obtaining material from the PVRUF RPV for the HSST Program. Several alternate arrangements for subcontracting the PVRUF RPV cut-up and transportation work were evaluated, and cost estimates were prepared and forwarded to the NRC HSST Project Manager who approved the plan for procurement of material for the PVRUF RPV and provided the necessary funding in a revision to the HSST Program SOW, on June 27, 1996. A bid package for the PVRUF cut-up subcontract was prepared and issued to potential vendors in September 1996.

S. S. Jang and M.-H. Song, of the Korea Institute of Nuclear Safety (KINS), visited the HSST Program facilities at ORNL on June 18, 1996. The visitors were shown the facilities in the fracture-toughness testing laboratory and given briefings on (1) ongoing research into shallow-flaw fracture toughness and the effects of both biaxial loading and cladding on shallow-flaw fracture toughness and (2) the FAVOR computer program for RPV deterministic and probabilistic RPV structural integrity assessments. W. Böhme, of the Fraunhofer Institut Werkstoffmechanik (IWM) in Freiburg, Germany, also visited ORNL on June 18, 1996. Progress in IWM's development of a local-approach-based ductile fracture-analysis methodology was discussed. An extended abstract was prepared

Executive

for an invited presentation entitled, "Recent Developments in the USNRC Heavy-Section Steel Technology Program," which is to be given at the United Kingdom Health and Safety Executive Structural Integrity Seminar in October 1996.

The ORNL HSST Program Manager (PM) is an active voting member of the American Society of Mechanical Engineers (ASME) Sect. XI, Working Group on Operating Plant Criteria (WGOPC), and the Task Group on Application of Nondestructive Examination (NDE) (TGANDE) to Plant Operating Criteria. The PM attended a meeting of the WGOPC in Louisville, Kentucky, on May 14, 1996, and meetings of the TGANDE and WGOPC in Portland, Oregon, on August 5 and 6, 1996. At the May meeting of the WGOPC, the ORNL HSST PM provided the WGOPC with an update on results from the ongoing HSST Program investigation of biaxial-loading effects on shallow-flaw fracture toughness. At the August meeting, he presented an HSST Program assessment of the safety factors inherent in the ASME Sect. XI, Appendix G, P-T curve analysis procedure. The HSST Program calculation included contributions to the SIFs from (1) stresses produced by through-wall temperature gradients from a uniform 100°F/h cool-down transient, (2) stresses produced by clad/base-material differential thermal expansion, (3) residual stress in the RPV structural welds, and (4) membrane stresses produced by pressure loading. Results from the analyses showed that the allowable pressure loading is significantly reduced when loading conditions (2) and (3) are included. The primary conclusion from the HSST Program analysis was that further evaluation of the Appendix G margins was required.

During the current reporting period, HSST Program personnel published seven papers in professional society publications and gave presentations to a joint DOE-NRC committee investigating core nuclear capabilities at DOE national laboratories, to the ASME Working Group on Operating Plant Criteria, and to an American Society for Testing and Materials (ASTM) committee.

2 Constraint Effects Analytical Development and Validation

The focus of this task is on the development and validation of dual-parameter, stress-based 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 RPV structural integrity evaluations are those associated with shallow flaws and biaxial loading. During the current reporting period, fabrication,

installation, and shakedown testing of the new large-scale biaxial test fixture was completed. The shakedown tests demonstrated that, with only minor modifications in some hardware, the new test fixture functioned to specifications. Fabrication of 16 of the intermediate-scale (100-mm-square beams) cruciform specimens was completed. Problems with (1) the electron-beam (EB) welder, which was out of service for about a month, and (2) a shortage of beam arms caused delays in the test specimen fabrication schedule. Problems with the EB welder have now been corrected, and an additional 24 beam arms have been fabricated. Fabrication of the intermediate-scale cruciform specimens is continuing at an accelerated rate, and completion is scheduled for early in the next reporting period.

Three cruciform specimens were tested during this reporting period at a nominal test temperature of 6°C (42°F) and under biaxial loading. The first test was performed under equibiaxial ($P_T/P_L = 1:1$) loading, where P_T and P_L are the applied transverse and longitudinal loads, respectively. This specimen failed at an estimated applied SIF of $152.5 \text{ MPa} \sqrt{\text{m}}$ ($138.7 \text{ ksi} \sqrt{\text{in.}}$). The other two specimens were tested under $P_T/P_L = 0.6:1$ biaxial loading and failed at estimated applied SIFs of 175.2 and $297.4 \text{ MPa} \sqrt{\text{m}}$ (159.4 and $270.6 \text{ ksi} \sqrt{\text{in.}}$), respectively. The specimens sustained substantial damage during failure with secondary cracks developing and propagating in the volume of material away from the initial flaw plane. Multiple cracking appears to be characteristic of cleavage failures generated under biaxial loading. Data from the cruciform specimen tests of heat-treated Plate 14 material continued to exhibit a strong influence of biaxial-loading ratio on shallow-flaw fracture toughness, with shallow-flaw fracture-toughness values from full biaxial ($P_T/P_L = 1:1$) loading tests being lower than values from uniaxial ($P_T/P_L = 0:1$) and biaxial ($P_T/P_L = 0.6:1$) tests for all three test temperatures investigated.

Analyses were performed to investigate the influence of loading conditions, test specimen size, and plastic zone boundary definitions on results predicted by the ORNL $K_J - \ln(R_{pl})$ dual-parameter, constraint-effects, fracture-toughness model. The current model uses a proportional limit stress (0% plastic strain) to define the plastic zone boundary and radius (R_{pl}). The first set of analyses investigated the impact on the $K_J - \ln(R_{pl})$ relationship when the plastic zone boundary was defined using 0%, 0.2%, 1%, and 5% plastic strain, respectively. The 0% plastic strain gave well-defined constraint-dependent $K_J - \ln(R_{pl})$ trajectories, while the 0.2% plastic strain definition gave constraint-dependent trajectories only at higher loads. At 1% strain levels, the $K_J - \ln(R_{pl})$ model shows essentially no constraint dependence. Results from this study showed

that all nonlinear strain must be included in the $K_J - \ln(R_{pl})$ dual-parameter, fracture-toughness correlation for the correlation to effectively reflect the influence constraint on shallow-flaw fracture toughness.

Analyses were also performed to investigate the influence on the constraint-effects predictions of the $K_J - \ln(R_{pl})$ dual-parameter, fracture-toughness correlation model of (1) biaxial-loading ratios in the range $0:1 \leq P_T/P_L \leq 2:1$ and (2) the length of the flaw in the width (B) direction. Block models with a 51-mm-deep (2.0-in.) straight-edge flaw ($a/W = 0.5$) were used in this investigation. Depth (W) and length (L) dimensions for both models were 102 mm (4 in.). The width (B) was set at 102 mm (4 in.) in one model and 254 mm (10 in.) in the other model. The models were loaded in uniaxial ($P_T/P_L = 0:1$) tension and in biaxial ($P_T/P_L = 1:1$ and $2:1$) tension. At low loads, the three loading conditions produced similar constraint-effects predictions, but at higher loads, at which loss of constraint was predicted for the uniaxially load case, a constraint condition equivalent to plane strain was retained under biaxial loading. Results for the 2:1 load case, using both models, showed constraint effects higher than those produced by a plane-strain condition and that the wider specimen was more constrained than the square specimen. These results indicate that the pressure-induced loading condition (which exists in a circumferential weld) produces a level of constraint greater than that existing in plane-strain uniaxially loaded, deep-flaw, single-edged-notched beam (SENB) specimens.

3 Evaluation of Cladding Effects

Stainless-steel cladding can influence the initiation and propagation of cracks from finite-length surface flaws by virtue of (1) the high toughness and low yield strength of the cladding material and (2) the heat-affected zone (HAZ) introduced in the RPV structural material by the cladding process. These effects are not reflected in existing RPV structural integrity analysis methods. Objectives for this task are to (1) experimentally determine the influence of cladding on crack initiation from finite-length surface flaws in a prototypical RPV biaxial stress field and (2) develop and validate the technology required for incorporation of those effects into RPV structural integrity assessment methods.

Fabrication and testing of the intermediate-scale clad cruciform specimens continued. These test specimens were fabricated from the same RPV-shell source material as that used for the full-thickness SENB clad beam. The finite-length surface flaws in these cruciform specimens have depth (a) and surface length (2c) dimensions of 19 mm and

53 mm, respectively. The test flaw was located in the shell-section structural weld and oriented with its major axis parallel to the direction of deposition of the weld material. Development tests of mockups of the shallow-flaw, clad-cruciform beam specimen design were performed to validate the specimen design and confirm the test procedures before initiating the clad-cruciform tests. Eight specimens in the HSST matrix of clad-cruciform beam experiments have been tested to date. Three of the specimens had the clad layer removed from the test section, and the cladding was kept in place on the remaining five specimens. This arrangement permitted the effects of cladding and the cladding-induced HAZ to be separated. The same surface flaw geometry was used for all specimens. The location of the cleavage-initiation site on the fatigued flaw front was determined by fractographic examination of the fracture surfaces for each of the eight specimens tested. Elastic-plastic, finite-element analyses (FEA) of the test specimens were performed to define the distribution of the applied SIF around the crack front as a function of the applied loading. The K_{Ic} value for each of the tests was determined using the analysis results together with the failure-load and cleavage-initiation-site test data. Fractographic data showed that at a test temperature of $T - RT_{NDT} = 3^\circ\text{C}$, cleavage initiation in the uniaxially loaded clad specimen occurred near the deepest point of the flaw, while cleavage initiation in the biaxial-loaded clad specimen occurred near the cladding-induced HAZ/weld interface. At higher test temperatures, cleavage initiation in both uniaxial- and biaxial-loading tests occurred near the deepest point of the flaw. Biaxial loading of both clad- and HAZ-type specimens resulted in lower toughness than was obtained under uniaxial loading, regardless of the location of the initiation site. The lowest toughness value obtained for the shallow-flaw, clad-type beam specimens under biaxial loading was only slightly above the ASME Sect. XI, Appendix A, K_{Ic} curve.

The RPV shell material from which the clad-cruciform specimens were machined contained residual stresses induced by the structural welding process and the cladding process. An analytical and experimental study was performed to determine the influence of residual stresses on the fracture-toughness values obtained from the clad-cruciform tests. Blocks of material were cut from the clad-shell segments and then machined to release the cladding layer. Inspections performed before and after the machining operation revealed that the shape change in the cladding resulted from its release from the shell segment structural material. FEA, using the cladding displacements as boundary conditions, showed that the average component of circumferential residual membrane stress in the cladding was 143.1 MPa (20.75 ksi) at room temperature. Inclusion of this residual stress in the test specimen analysis

Executive

produced an additional K_I increment of $4.4 \text{ MPa}\sqrt{\text{m}}$ ($4 \text{ ksi}\sqrt{\text{in.}}$). An analytical evaluation of the effect of residual stresses in the shell segment structural weld showed that these stresses were effectively eliminated when the intermediate-scale, clad-cruciform specimens were machined from the full-thickness shell material. Residual stresses, therefore, have a relatively minor impact on results from tests on the intermediate-scale clad-cruciform test specimens.

Drop-weight, tensile, and hardness characterization tests were performed on material from the longitudinal welds of the RPV shell segments. The nil-ductility temperature (NDT) for the longitudinal weld was determined to be -59°C . Hardness tests showed that the cladding-induced HAZ of the RPV shell material may have hardness numbers that are well above those for those of adjacent cladding and base materials. The hardness mismatch at the interface between the HAZ material and material on either side of the HAZ could generate local strain concentrations in the crack-tip plastic zone because the crack front passes through the interface zones. Postinstallation shakedown testing of the new large-scale biaxial test fixture was completed.

Severe transient loading, such as that imposed during a PTS event, is predicted to cause plastic deformation of the cladding material in an RPV. Probabilistic analyses, such as those performed by the FAVOR computer program, require the analysis of very large numbers of loading cases. The conventional elastic-plastic finite-element approach to the analysis of inelastic behavior is, therefore, impractical for application to probabilistic RPV integrity evaluations. Neglect of clad plasticity effects introduces an excessive element of conservatism into the probabilistic fracture analysis. Work was initiated, therefore, to develop and validate a computational superposition model that more nearly approximates the effects of clad plasticity in probabilistic RPV analyses. A number of approaches to approximating the nonlinear stress-strain response of the cladding were evaluated using a simple cracked-beam model. A pseudoelastic nominal stress approach based on an equivalent strain energy method proposed by Merkle gave the best results. Development of this analytical model is continuing.

4 Ductile-to-Cleavage Fracture-Mode Conversion

This task investigates the effect of prior ductile tearing on cleavage-fracture-initiation toughness. Ductile tearing before 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 to take into account the effects of any precleavage ductile tearing in fracture predictions. Two models for the prediction of ductile tearing were evaluated: (1) a computational cell model in which dilatant-plasticity-based microvoid growth and coalescence models are used to predict material softening ahead of the crack tip and (2) a model in which the crack-tip opening angle (CTOA) is used as the measure of plastic deformation ahead of the crack tip. Tearing is predicted to occur when plasticity in the material ahead of the crack tip reaches a predetermined critical level. The ductile tearing models were incorporated into the finite-element computer program WARP3D, which is under development at UI. Parameters for the computational cell and CTOA models were defined to bring the predicted R-curves into agreement with the experimental data for an $a/W = 0.6$ specimen. The calibrated models were then used to predict the R-curve for an $a/W = 0.14$ specimen, for which test data were available. The computational cell model predicted an R-curve that was in good agreement with experimental data for the $a/W = 0.14$ specimen, whereas the CTOA model significantly underestimated the actual tearing resistance. The computational cell model was selected for further development. The two principal material parameters required for the computational cell model are the initial void volume fraction, f_0 , and the characteristic length, D , associated with crack-plane elements. R-curve data from side-grooved 1T compact tension (CT) specimens have been used to generate f_0 and D values for weld and base material taken from an RPV shell segment. In the next phase of development, these values will be used to predict the tearing response of a shallow flaw in a full-thickness specimen cut from an RPV shell, and the analytical predictions will be then compared with results from tests of this specimen.

5 Fracture-Analysis Methods Development and Application

Development of the 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 configuration control of the program possible.

In this reporting period the library of stress-intensity influence coefficients (SIFICs) for inner-surface flaws in prototypical domestic commercial pressurized-water reactor (PWR) and boiling-water reactor (BWR) vessel geometries was extended to include SIFICs for a range of axially and circumferentially oriented inner-surface flaws in clad RPVs with $(R_i/t) = 20$. This geometry is prototypical of a high percentage of the domestic commercial BWR vessels. Implementation of this data base of SIFICs into the FAVOR code was validated.

Analyses were performed to evaluate revisions to Regulatory Guide (RG) 1.154, which are currently under consideration by NRC. RG 1.154 defines acceptable methods of analysis to be used for the evaluation of RPV integrity under PTS loading. The potential revisions to RG 1.154 would (1) designate as method 1 the current analysis method, which requires that all flaws be conservatively assumed to be inner-surface breaking flaws, and (2) add a method 2 analysis option, which would reduce some of the method 1 conservatism by permitting representation of the spatial distribution of flaws within the thickness of the RPV wall. The method 2 approach uses the RR-PRODICAL computer code, an expert-based program developed by Rolls Royce, plc, in the United Kingdom, to predict flaw densities, flaw sizes, and distribution of flaws through the thickness of welds in an RPV wall. A software interface was developed to permit output data files from the RR-PRODICAL program to be input into a specialized version of the FAVOR code. PTS analyses were performed to test the PRODICAL-FAVOR software interface and results generated by the method 1 and method 2 analysis options. The stylized PTS thermal transient used in the analysis assumed an exponentially decaying coolant temperature. End-of-service-life irradiation embrittlement conditions were assumed by the method 1 and method 2 analyses, respectively. The permissible RPV, operating life as determined by the method 2 analysis, was ~27% longer than the permissible operating life determined using the method 1 analysis.

Support was provided to an NRC review of licensee probabilistic fracture mechanics analysis results, and analyses were performed to support the work of the ASME Sect. XI WGOPC.

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 the current reporting period,

testing and analyses for the A 302 Grade B (modified) material J-R evaluation was completed, and Vol. 1 of the final report was written and submitted to NRC. Dynamic fracture-toughness testing of A 533 B material and interpretive analyses were completed by the Naval Service Warfare Center under an HSST Program subcontract, and a draft of the final report was delivered to ORNL for review.

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 and in the HSSI 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 evaluating and resolving RPV structural integrity issues.

During the current reporting period, an evaluation was performed to resolve an issue relating to the value of the constraint parameter (M) incorporated into the ASTM draft standard, "Test Method for the Determination of Reference Temperature, T_0 , for Ferritic Steels in the Transition Range." Lower-bound fracture-toughness values (J_0) are obtained in tests during which a high level of crack-tip constraint is maintained and failure occurs under conditions of small-scale yielding. Under conditions of reduced crack-tip constraint, the level of plastic deformation at failure increases with an associated increase in the average fracture toughness (J_{avg}). Because the predicted value of T_0 is dependent on the measured value of J_{avg} , the ASTM team developing the T_0 standard set a limit of 1.2 on the ratio J_{avg}/J_0 to prevent the generation of nonconservative T_0 estimates. The intent of the constraint parameter M is to ensure compliance with $J_{avg}/J_0 \leq 1.2$ criteria. Analyses performed by a team led by Professor Dodds at UI have shown that, at a constant value of M , the ratio J_{avg}/J_0 increases as the location of the cleavage crack initiation site moves farther from the crack tip. Fractographic data from tests performed at the National Institute for Standards and Technology in Rockwell, Maryland, by J. Heerens et al. of the GKSS Research Center in Germany provided crack initiation site data for a material similar to A 533 B RPV steel. The HSST research team used these analysis and test results to show that the $J_{avg}/J_0 \leq 1.2$ criteria were satisfied at the crack initiation site location using a value of 30 for the constraint parameter M . This M value has now

Executive

been adopted for the transition range T_0 standard. The fractographic data used in the analysis to determine M were not screened to eliminate data points that included prior ductile tearing. The issue of prior ductile tearing will be addressed at a later time by the ASTM team.

Heavy-Section Steel Technology Program Semiannual Progress Report for April–September 1996

1 Program Management*

W. E. Pennell

1.1 Objectives

The Heavy-Section Steel Technology (HSST) Program is conducted for the U.S. Nuclear Regulatory Commission (NRC) by Oak Ridge National Laboratory (ORNL). The focus of the program is on the development and validation of technology to assess 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. These are (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 to resolve 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 Applications divisions at ORNL. Subcontracts with consultants, universities, and other research laboratories are used to gain access to special expertise and capabilities

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

ORNL-DWG 97-142011 EFG

Figure 1.1 HSST Program Level 1 work breakdown structure

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 and Program Direction

Preparation of the final version of the latest revision to the HSST Program 189 was completed. The SOW issued by the NRC to give direction for preparing the final version of the 189 differed from the SOW issued for the preliminary version of this 189. The differences were in Tasks H1 and H5. In Task H1 a requirement was added for material to be obtained from the beltline section of the RPV for the Pressure Vessel Research User's Facility (PVRUF). This material is to be used for the manufacture of future test specimens. In Task H5, the requirement for the generation of stress-intensity factor influence coefficients (SIFICs) for RPVs with an R/t ratio of 15 was deleted, and requirements for modification of the FAVOR computer program to perform the method 1 (best estimate) and method 2 (probabilistic risk assessment) analyses were added, together with a requirement to perform probabilistic

Figure 1.2 Resources applied to the HSST Program research and development tasks

fracture-mechanics analyses of two plants. All required changes to the HSST Program plan were made, and the completed 189 was forwarded to the ORNL Engineering Technology Division finance officer for review and approval.

A report on the core competency capabilities existing at ORNL in the area of reactor vessel integrity assessment was prepared at the request of the ORNL NRC Programs Office. The report covered capabilities existing in the HSST, the Heavy-Section Steel Irradiation (HSSI), and the Embrittlement Data Base and Dosimetry Evaluation programs. The ORNL HSST Program Manager (PM) presented this material to the joint NRC-DOE committee investigating core nuclear capabilities at DOE national laboratories. The presentation occurred at the NRC White Flint-2 offices on April 16, 1996.

Notification was received from the ORNL NRC Programs Office that personnel would visit ORNL in the near future for a review of the NRC Office of Nuclear Regulatory Research programs. A format for the HSST Program presentation at this review meeting was prepared and reviewed with the ORNL Director of NRC Programs. The presentation provided an overview of the objectives; planned activities; accomplishments; cost, schedule, and performance; and technical highlights for each of the program tasks for the FY 1995 and 1996 performance period.

1.4 Subcontracts and Materials Procurement

Research subcontracts were in progress with the University of Maryland (UM), the University of Illinois (UI), and the Carderock Division of the Naval Surface Warfare Center (CDNSWC). A consulting subcontract was in progress with Professor S. T. Rolfe of the University of Kansas. The UM, UI, and CDNSWC research has application to the development of fracture-toughness constraint-effects models; tearing-cleavage, mode-conversion models; and dynamic fracture initiation models, respectively. During the current reporting period, all three subcontractors experienced unanticipated difficulties and requested, and were granted, no-cost extensions of their subcontracts.

UM is examining fracture surfaces from HSST Program test specimens to determine fracture initiation locations and the extent of the crack-tip plastic zone and is also conducting analytical studies. Discussions were held with UM personnel to complete the details of the crack-tip analysis to be performed. UM will perform an analytical investigation of the physical basis for the link between crack-tip-opening displacement (CTOD) and plastic zone radius (R) under biaxial loading. Results from this work will provide technical support for the $K_{Jc} - \ln R$ strain-based, dual-parameter, fracture-toughness correlation, which is being developed in the HSST Program.

UI is enhancing and verifying computer software to model ductile crack growth using a Guron Model. All ORNL reviews were completed on a letter report by A. Gullerud and R. H. Dodds, Jr., of UI entitled *Finite-Element Modeling of Ductile Crack Growth*. A draft copy of the letter report was forwarded to the NRC HSST Project Manager for review and comment before it is released for publication.

CDNSWC is performing dynamic fracture initiation toughness tests of RPV steel. Advanced copies of a report by R. E. Link of the U.S. Naval Academy and S. M. Graham of the Vector Research Company, Inc., entitled *Dynamic Fracture Initiation Toughness of ASTM A533, Grade B Steel Plate*, NSWCCD-TR-61-96/01, were received from CDNSWC in August 1996. Copies of the report were distributed to HSST Program personnel in HSST Program Tasks 4, 5, and 6 for review and comment.

A SOW was prepared for the consulting services of S. T. Rolfe for the period June 17, 1996, to June 16, 1997, and the associated contract documentation was processed.

Developments in the HSSI Program plan for obtaining material from RPV welds necessitated the investigation of alternate approaches to obtaining material from the PVRUF RPV for the HSST Program. The HSSI Program located material from the RPV from the closed Shoreham nuclear plant. This material may be more appropriate for planned HSSI research than material from the PVRUF RPV. The previously planned joint HSST-HSSI initiative for securing material from the PVRUF RPV had, therefore, to be revised. PVRUF RPV material identified for the HSST Program is intended for use as feedstock for the fabrication of future large-scale fracture-toughness test specimens. Several alternate arrangements for subcontracting the PVRUF RPV cut-up and transportation work were evaluated, and cost estimates were prepared and forwarded to the NRC HSST Project Manager. The cost estimates were in the form of baseline estimates for procuring material required for the HSST Program and an incremental cost for providing material that Pacific Northwest National Laboratory (PNNL) personnel required to complete their evaluation of results from nondestructive examinations (NDE) made of the PVRUF RPV welds using an advanced ultrasonic examination technique. Provision was also made in the PVRUF cut-up plan for the HSSI Program to receive four segments from the RPV beltline weld. The NRC HSST Project Manager approved the plan for procurement of material for the PVRUF RPV and provided the necessary funding in a revision to the HSST Program SOW, dated June 27, 1996. A bid package for the PVRUF cut-up

subcontract was prepared and issued to potential vendors in September 1996.

1.5 International Interactions

S. S. Jang and M.-H. Song, of the Korea Institute of Nuclear Safety (KINS), visited the HSST Program facilities as a part of an NRC-sponsored visit to ORNL on June 18, 1996. Their particular interest was in technology for the structural integrity assessment of aging nuclear RPVs with flaws. The visitors were shown the facilities in the fracture-toughness testing laboratory and given (1) a briefing on the ongoing research into shallow-flaw fracture toughness and the effects of both biaxial loading and cladding on shallow-flaw fracture toughness and (2) a briefing on and a demonstration of the FAVOR computer program for RPV deterministic and probabilistic RPV structural integrity assessments. The visitors indicated their intention to explore with NRC the potential for further interaction between KINS and the HSST Program.

W. Böhme, of the Fraunhofer Institut Werkstoffmechanik (IWM) in Freiburg, Germany, also visited ORNL on June 18, 1996. In a brief meeting with Böhme, progress was discussed concerning the IWM development of a local-approach-based, ductile-fracture analysis methodology, using material property input data that can be derived from material in the existing RPV surveillance programs. 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 . When an NRC-ORNL team visited IWM in June 1995, IWM had developed a means of estimating the constraint-dependent J-R curves using data from relatively small uniform-diameter tensile specimens. The intended means for validation of the method was by synthesization of a J-R curve that matched the J-R curve obtained from tests of precracked and side-grooved Charpy specimens. At the time of the NRC-ORNL team visit, IWM had reported difficulty in matching the J-R curve from the precracked Charpy specimens. The group discussed the possible role of temperature gradients produced in the gauge length of the miniature tensile specimens during the dynamic tests. Böhme reported that the analysis of thermal stresses in the test section of the dynamic miniature tension specimens had not yet been performed. Efforts to improve the level of agreement between the synthesized J-R curve and the curve derived from the precracked Charpy specimens was continuing, however, and Böhme anticipated that the analysis of dynamic-heating-induced thermal stresses could be included in that ongoing effort.

Program

Foreign travel recommendations for HSST Program personnel for FY 1997 were prepared and forwarded to the NRC HSST Project Manager. An extended abstract was prepared for an invited presentation entitled, "Recent Developments in the USNRC Heavy-Section Steel Technology Program," to be given by the HSST PM at the United Kingdom Health and Safety Executive Structural Integrity Seminar in October 1996.

1.6 Codes and Standards

The ORNL HSST PM is an active voting member of the American Society of Mechanical Engineers (ASME) Sect. XI Working Group on Operating Plant Criteria (WGOPC), and the Task Group on Application of NDE (TGANDE) to Plant Operating Criteria. He attended a meeting of the WGOPC in Louisville, Kentucky, on May 14, 1996, and meetings of TGANDE and WGOPC in Portland, Oregon, on August 5 and 6, 1996.

The WGOPC is collecting data in support of an overall review, and potential update, of Appendix G of ASME Sect. XI. Data have been collected on cladding effects, shallow-flaw effects, and pop-in and local brittle-zone effects. At the May meeting of the WGOPC, the ORNL HSST PM provided the WGOPC with an update of results from the ongoing HSST Program investigation of biaxial-loading effects on shallow-flaw fracture toughness.

The WGOPC is evaluating a proposal that the definition of the reference flaw in Appendix G to Sect. XI be modified to permit the orientation of the reference flaw to be dictated by the orientation of the critical RPV welds. The ultimate goal for this proposal is for Appendix G to be modified to permit the evaluation of flaws in circumferential welds to be based on flaws aligned with the plane of the weld. Discussions of this topic resulted in the identification of areas in which additional information was required to assess the technical justification for and potential benefits from the proposed Appendix G change. The ORNL HSST PM was assigned responsibility for providing the WGOPC with any available published information on (1) fracture toughness from biaxial tests in which the stress ratio was prototypical of the stress ratios found in circumferential flaws and (2) results from fracture-toughness tests on finite-length flaws in which fracture toughness varied along the length of the crack front.

At its August meeting, the WGOPC discussed the safety factors inherent in the ASME Sect. XI, Appendix G, proce-

dures for defining the P-T curve for a reactor vessel. The WGOPC had previously agreed that an understanding of the safety factors was an important element of the technical basis for any proposed future modifications to Appendix G. The issue came up at this meeting in connection with the proposed Appendix G modification discussed in the previous paragraph. The ORNL HSST PM presented an HSST Program assessment of the safety factors. The HSST Program calculation included contributions to the stress-intensity factors (SIFs) from (a) stresses produced by through-wall temperature gradients from a uniform 100°F/h cooldown transient, (b) stresses produced by clad-base-material differential thermal expansion, (c) residual stress in the RPV structural welds, and (d) membrane stresses produced by pressure loading. Pressure on the crack face, which generates a small addition to the SIF, was not included in these calculations. Appendix G requires that loading conditions (a) and (d) be included in the analysis used to define the P-T curve but does not require that loading conditions (b) and (c) be included.

Appendix G compensates for the missing loading conditions by requiring that a conservative flaw size ($1/4T$) be used in the analysis, together with a safety factor of 2 on the applied pressure loading. The analyses presented gave the allowable pressure, based on the K_{IR} fracture-toughness curve, when the missing loading conditions were included in the analysis. Estimates for the residual stresses and the clad-base-material differential expansion stresses were based on previously reported HSST Program data obtained during the fabrication of the full-scale clad-beam specimens from shell segments taken from the RPV from a canceled nuclear plant. The analyses were performed for the $1/4T$ reference flaw and two shallower flaws. Results from the analyses, with all four loading elements included, showed that (1) decreasing the reference flaw size has very little influence on the SIF produced by the strain-controlled loading [loading conditions (a) plus (b) plus (c)], (2) the K_{IR} value decreased as the flaw depth decreased because of the lower temperatures and higher RT_{NDT} values at the crack-tip for the shallower inner-surface flaws, and (3) the allowable pressure loading is significantly reduced when loading conditions (b) and (c) are included in the analysis. The cooldown transient considered in this analysis was conservative in that it did not consider the decrease in the cooldown rate that would normally occur at lower reactor system temperatures. The primary conclusion from the HSST Program analysis was that further evaluation of the Appendix G margins was required.

1.7 Reports, Papers, and Presentations

W. E. Pennell presented a paper entitled, "Evaluating the Safety of Aging Nuclear Reactor Pressure Vessels," at the 58th Annual American Power Conference, in Chicago, Illinois, on April 9, 1996. Notification was received that a paper by W. E. Pennell and S. N. M. Malik entitled, "Structural Integrity Assessment of Aging Nuclear Reactor Pressure Vessels," has been accepted for publication in *Nuclear Engineering and Design* (NE&D). During the current reporting period, HSST Program personnel published seven papers in professional society publications,¹⁻⁷ gave a presentation to a joint DOE-NRC committee investigating core nuclear capabilities at DOE national laboratories,⁸ and gave presentations at an American Society for Testing and Materials committee meeting,⁹ and at meetings of the ASME Sect. XI Working Group on Operating Plant Criteria.¹⁰⁻¹¹

References

1. D. E. McCabe, E. T. Manneschildt, and R. L. Swain, "Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials Data Analysis, *Proc. 58th Annual American Power Conference*, Vol. 58, Chicago, April 9, 1996.
2. W. E. Pennell, "Evaluating the Safety of Aging Nuclear Reactor Pressure Vessels," *Proc. 58th Annual American Power Conference*, Vol. 58, Chicago, April 9, 1996.
3. J. A. Keeney, "Fracture Assessment of Weld Material from a Full-Thickness Clad RPV Shell Segment," *Proc. 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996*.
4. W. J. McAfee et al., "Biaxial Loading Effects on Shallow-Flaw Fracture Toughness in Reactor Pressure Vessel Steels," *Proc. 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996*.
5. D. E. McCabe and J. E. Merkle, "Estimation of Lower-Bound K_{Jc} on Pressure Vessel Steels from Invalid Data," *Proc. 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996*.
6. J. G. Merkle, "Patterns and Perspectives in Applied Fracture Mechanics," *Fracture Mechanics, Vol. 26, ASTM STP 1256*, W. G. Reuter, J. H. Underwood, and J. C. Newman, Jr. Eds. (American Society for Testing and Materials, Philadelphia, 1995).*
7. B. R. Bass, J. W. Bryson, T. L. Dickson, W. J. McAfee, and W. E. Pennell, "Development of a Shallow-Flaw Fracture Assessment Methodology for Nuclear Reactor Pressure Vessels," pp. 211-218 in *Life Assessment and Life Extension of Engineering Plant, Structures and Components*, eds., J. H. Edwards, P. E. J. Flewitt, B. C. Gasper, K. A. McLarty, P. Stanley, and B. Tomkins, *Proc. Third International Conference on Engineering Structural Integrity Assessment, Churchill College, UK, September 1996*, Engineering Materials Advisory Services, 339 Halesowen Road, Cradley Heath, Warley, B64 6PH, United Kingdom.
8. W. E. Pennell, W. R. Corwin, and J. V. Pace III, "Structural Integrity Materials and Irradiation Effects Embrittlement Data Base and Dosimetry Evaluations," presented at a meeting to the joint DOE-NRC committee investigating core nuclear capabilities at DOE national laboratories, at White Flint-2, Washington, D.C., April 16, 1996.
9. D. E. McCabe, "Overview of Proposed ASTM Test Method for the Determination of Reference Temperature T_0 for Ferritic Steels in the Transition Range," presented at the ASTM Committee E08 meeting, Orlando, Florida, May 20-23, 1996.
10. W. E. Pennell, "Biaxial Loading Effects on Shallow-Flaw Fracture Toughness," presented to the ASME Sect. XI Working Group on Operating Plant Criteria at the Galt House Hotel, Louisville, Kentucky, May 14, 1996.
11. W. E. Pennell, "Factors of Safety Inherent in the ASME Sect. XI, Appendix G, Procedure for Defining the Reactor Vessel P-T Curve," presented to the ASME Sect. XI Working Group on Operating Plant Criteria at the Portland Hilton Hotel, Portland, Oregon, August 6, 1996.

*Available from 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 comprises 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. The current effort under subtask 2.1 addresses the effects of biaxial loading on fracture toughness using an A 533 Grade B material that has been heat-treated to achieve a yield stress comparable to highly irradiated, that is, end-of-life RPV material. The results of a preliminary set of tests in a defined matrix of intermediate-scale specimens were reported in the previous semiannual. Testing of this matrix investigating a wider range of temperatures is continuing with good results being achieved in identifying the effect of biaxial loading on fracture toughness. These results are reported and discussed here.

2.1 Biaxial-loading Effects on Constraint (W. J. McAfee, B. R. Bass, J. W. Bryson, and W. F. Jackson)

2.1.1 Experimental Developments in Biaxial Test Program (W. J. McAfee, B. R. Bass, and W. F. Jackson)

2.1.1.1 Installation of New Large-Scale Biaxial Test Fixture

The fabrication and installation of the new large-scale biaxial test fixture, which have been described previously,¹ were completed during the previous reporting period. Installation of the fixture in the test machine went very smoothly. Some problems that were related to high-strength bolts were encountered when the pistons and load seats were mounted on the platen. The specified bolts were commercial, high-strength 1.5-in.-diam with a fine thread. It appears that these bolts were heat-treated after the threads were applied. Consequently, a slight change in the thread pitch resulted. Although within specifications for the bolts, because the threads would not engage the machined threads in the platen, the bolt threads had to be "chased" to achieve a fit. This additional effort resulted in a minor delay in restarting the test program. Postinstallation shake-down testing of this new fixture was performed to work out any design "bugs" and to demonstrate full functionality. For example, the new 890-kN (200-kip) reactor pistons

now being used have a larger bore and stroke than the 534-kN (120-kip) pistons that they replaced. For the same amount of piston travel, a larger volume of hydraulic fluid must be displaced, thus requiring a larger-capacity (oil) divider piston to accommodate the increased oil displacement volume. A new divider piston was acquired and installed before testing startup. One effect of the newer, larger piston has been a minor change in the load biaxial ratio for the new fixture. The new piston has a slightly different piston area ratio than the one it replaced, that is, 0.54 (new) vs 0.56 (old), yielding a very slightly reduced biaxial ratio. This difference has no effect for uniaxial and full biaxial (1:1) testing and very little effect for what has nominally been the 0.6:1 biaxial tests. The 0.6:1 ratio is taken from actual pressure measurements across the divider piston so that it was very easy to verify the load ratio achieved using the new piston. The results for one of the first 0.6:1 tests performed using this new setup is shown in Fig. 2.1. The applied biaxial-load ratio at failure was 0.57 and was consistent between both the pressure ratio and the beam arm strain ratio. The complete sequence of shake-down tests demonstrated that, with only minor modifications in some hardware, the new fixture functioned to specifications.

2.1.1.2 Specimen Fabrication

Specimen fabrication is continuing. Currently, 16 specimens have been completed. By "completed," it is meant that the specimen fabrication sequence has been finished and that the specimen is ready for instrumentation and failure testing. Problems were encountered during this period that delayed fabrication and, subsequently, testing. These problems stemmed from the EB welder being out of service for about a month. The lack of the EB welder created a backlog of work for both this program and other programs relying on EB welding services. When the welder was returned to service, priorities were established among the competing programs to try to address the immediate needs of each. The HSST Program could not get as much time as was desired to EB weld the beams available. This delay was somewhat further exacerbated by the fact that both HSST Task H2 (constraint cruciform beams) and HSST Task H3 (clad cruciform beams) were in effect competing with each other for machine time. The problem was compounded further by the unavailability of beam arms for fabrication after the welder was back in service. Several beams had been fabricated into the uniaxial configuration, which uses two beam arms, and had been fatigue-precracked. Usually at this stage, the specimen will be final welded, instrumented, tested to failure, and disassembled.

Figure 2.1 Demonstration of nominal 0.6:1 biaxial-loading capability for new large-scale cruciform test fixture. Comparison of beam arm strain, beam arm strain ratio, and pressure ratio vs longitudinal bending moment for failure test of shallow-flaw cruciform specimen P6A shows consistency of applied biaxial load.

Then four beam arms are freed for fabrication of the next specimen(s). With only a limited number of specimens available for testing, only a few beams arms were available for recycling. This was identified early on as a problem that could further slow the fabrication and testing sequence even after the EB welder was back in service. To alleviate the problem, additional beam arms were fabricated. An advantage was taken of a small stock of heat-treated A 533 material left over from a previous non-NRC program. An additional 24 beam arms were machined. This number is sufficient to ensure a continuous supply of components for the HSST cruciform beam tests.

Currently, fabrication is continuing at an accelerated rate with the expectation that all cruciform specimens in this sequence will be completed early during the next reporting period.

2.1.2 Cruciform Testing and Results (W. J. McAfee, J. W. Bryson, and W. F. Jackson)

Three cruciform specimens were tested to failure during this reporting period, and the results are summarized in Table 2.1. These specimens were used to investigate the shape of the toughness curve through the transition temperature region for this material. From the tests performed at -5°C (22°F), it was concluded that there would be a low

probability of achieving cleavage initiation under uniaxial loading for temperatures greater than -5°C (22°F). These tests were thus performed under biaxial-loading conditions only at a nominal test temperature near 6°C (42°F). Although this test temperature is only an increment of 11°C up the transition curve from the previous set [-5°C (22°F)], a conservative approach was taken because of the change in fracture response between -30°C and -5°C (22°F), that is, a change from near-elastic response with no demonstrated biaxial-loading effects to demonstration of a significant biaxial-loading effect at a temperature only 25°C higher. Specimen P4B was tested at 6°C (42°F) under biaxial (1:1) loading and failed at a longitudinal moment of 994 kN (223.5 kips), resulting in an estimated crack-mode-opening-displacement (CMOD) toughness of $152.5 \text{ MPa}\sqrt{\text{m}}$ ($138.7 \text{ ksi}\sqrt{\text{in.}}$). The deformation response was consistent with the biaxial (1:1) specimens tested near -5°C (22°F) in that only a small amount of plasticity was accumulated. This is illustrated in the longitudinal moment vs CMOD and longitudinal moment vs load-line displacement (LLD) plots, as shown in Figs. 2.2 and 2.3, respectively. Note that longitudinal moment is now being used as the loading parameter in presentation of results. With the new test fixture, the moment arm length can be changed from test to test, if required. The bending moment applied to the test section can then be different between two specimens with the same total load. Also, all of the finite-element analyses (FEA) have been and are

Table 2.1 Summary of heat-treated plate 14 cruciform specimen test results

Specimen number	Test temperature [°C (°F)]	Biaxiality ratio	Failure moment [kN-m (kip-in.)]	Failure LLD [mm (in.)]	Failure CMOD [mm (in.)]	KJ from P-LLD [MPa√m (ksi√in.)]	KJ from P-CMOD [MPa√m (ksi√in.)]
P2A ^a	38 (100)	0:1	226.7 ^b (2006.5)	29.561 (1.1638)	2.0269 (0.0798)	NA ^c	NA ^c
P12B	-30 (-22)	0:1	81.08 ^b (717.7)	1.938 (0.0763)	0.1372 (0.0054)	76.3 (69.5)	88.0 (80.1)
P3A	-31 (-24)	0:1	93.5 ^b (827.4)	2.276 (0.0896)	0.1753 (0.0069)	97.2 (88.4)	103.0 (93.7)
P14B	-31 (-24)	0.6:1	64.6 ^b (571.3)	1.504 (0.0592)	0.1118 (0.0044)	99.6 (90.7)	70.3 (64.0)
P7B	-30 (-22)	0.6:1	122.7 ^b (1085.7)	2.802 (0.1103)	0.2362 (0.0093)	129.2 (117.6)	136.3 (124.0)
P13B	-28 (-18)	1:1	106.7 ^b (944.7)	2.398 (0.0944)	0.2032 (0.0080)	97.9 (89.1)	112.1 (102.0)
P4A	-29 (-21)	1:1	130.4 ^b (1154.1)	2.936 (0.1156)	0.2946 (0.0116)	139.7 (127.1)	141.2 (128.5)
P9A	-3 (26)	0:1	194.2 ^b (1718.8)	14.575 (0.5738)	1.6916 (0.0666)	354.3 (322.5)	394.5 (359.1)
P2B.2	-3 (26)	0:1	191.1 ^b (1691.1)	13.774 (0.5423)	1.4173 (0.0558)	346.2 (315.1)	375.5 (341.8)
P3B	-4 (24)	0.6:1	184.1 ^b (1629.45)	8.440 (0.3323)	0.7645 (0.0301)	266.1 (242.2)	266.8 (242.8)
P12A	-6 (21)	0.6:1	171.4 ^b (1516.9)	5.923 (0.2332)	0.5715 (0.0225)	212.5 (193.4)	223.6 (203.5)
P15A	-4 (25)	1:1	136.2 ^b (1205.7)	3.424 (0.1348)	0.3150 (0.0124)	155.6 (141.6)	153.9 (140.1)
P6B	-4 (24)	1:1	139.2 ^b (1232.1)	3.637 (0.1432)	0.3505 (0.0138)	166.9 (151.9)	162.3 (147.8)
P4B	6 (42)	1:1	142.1 ^b (1257.4)	4.790 (0.1886)	0.3555 (0.0132)	183.2 (166.7)	167.5 (152.5)
P6A	4 (40)	0.6:1	159.4 ^d (1410.8)	5.227 (0.2058)	0.3988 (0.0157)	184.1 (167.6)	172.4 (156.9)
P10A	8 (46)	0.6:1	199.5 ^d (1765.8)	10.691 (0.4209)	0.8763 (0.0345)	308.1 (288.4)	297.4 (270.6)

^aTest discontinued, no cleavage.^bMoment arm = 254 mm.^cNA = not applicable.^dMoment arm = 286 mm.

Figure 2.2 CMOD for failure test of cruciform specimen P4B, $P_T/P_L = 1:1$, $T_f = 6^\circ\text{C}$ (42°F)

Figure 2.3 LLD for failure test of cruciform specimen P4B, $P_T/P_L = 1:1$, $T_f = 6^\circ\text{C}$ (42°F)

being performed using a model with the load applied at a fixed distance from the specimen center. For both of these reasons, a change to longitudinal bending moment ensures that consistency is maintained in comparing results between tests and the results of tests using FEA. The fracture surface for this specimen is shown in Fig. 2.4. Investigation of the fatigue precrack flaw front reveals the possible existence of two initiation sites. What is probably the primary site is located 15 mm from the specimen centerline (to right of center in photo), at which point the fatigue precracked flaw is ~11 mm deep. The second site is located 25 mm from the centerline where the flaw is 10 mm deep. The fracture surface shows multiple cracks, some of which propagated in the longitudinal direction, that is, normal to the flaw plane. This tendency for the propagating flaw to bifurcate both in the through-thickness direction and the transverse direction seemed to be a characteristic unique to the biaxially loaded specimens. Flaw propagation for the

uniaxially loaded specimens tended to run a single flaw front most of the way through the thickness of the test section.

The second specimen tested was specimen P6A. Test conditions were 4°C (40°F) under biaxial (0.6:1) loading. The specimen failed at a longitudinal moment of 159.4 kN-m (1411 kip-in.), resulting in an estimated (CMOD) toughness of $175.2 \text{ MPa}\sqrt{\text{m}}$ ($159.4 \text{ ksi}\sqrt{\text{in.}}$). The longitudinal moment vs CMOD and vs LLD plots are shown as Figs. 2.5 and 2.6, respectively. The toughness obtained from this test was lower than what was expected from the 0.6:1 tests performed at -5°C (22°F). From other test programs, it has been observed that, as the test temperature is increased, the scatter in the resulting toughness data increases. Some additional scatter in these test results would then be expected with increasing test temperature.

ORNL-DWG 97-142016 EFG

Figure 2.4 Fracture surface of cruciform specimen P4B showing location of two apparent initiation sites

Figure 2.5 CMOD for failure test of cruciform specimen P6A, $P_T/P_L = 0.6:1$, $T_f = 4^\circ\text{C}$ (40°F)

Figure 2.6 LLD for failure test of cruciform specimen P6A, $P_T/P_L = 0.6:1$, $T_f = 4^\circ\text{C}$ (40°F)

The fracture surface for this specimen is shown in Fig. 2.7. What appears to be the primary site is located ~8 mm from the specimen centerline (to left of center in photo). The fatigue precracked flaw at this location is ~12 mm deep. In comparison to Fig. 2.4, the fracture surface shows fewer fracture surfaces, although there is still a tendency for the propagating flaw to rotate away from the plane of the initial flaw. For example, the edges of the flaw shown have curved away from the initial flaw plane (into the plane of the paper). Along the center of the test section, the flaw propagated through the thickness at an angle between 10° to 15° from the vertical. The test section is not characterized by general cracking in the longitudinal direction as was the case for the biaxial (1:1) loaded specimen, which was shown in Fig. 2.4.

For confirmation purposes, the third cruciform specimen tested during this period was at the same conditions as those of specimen P6A. Specimen P10A was tested at 8°C (46°F) under biaxial (0.6:1) loading; failure occurred

at a longitudinal moment of 199.5 kN-m (1766 kip-in.). The estimated toughness (CMOD) was 297.4 MPa $\sqrt{\text{m}}$ (270.6 ksi $\sqrt{\text{in.}}$). The deformation behavior of this specimen is shown in Figs. 2.8 and 2.9. The amount of plastic deformation measured for this test was substantially greater than that measured for specimen P6A, also under 0.6:1 biaxial loading and at nearly the same temperature, and is reflected in the comparative values of toughness obtained. The fracture surface for this specimen is shown in Fig. 2.10. The initiation site is located ~25 mm to the left of the specimen centerline, as shown in the figure. The specimen underwent substantial damage during failure; secondary cracks developed and propagated in the volume of material away from the initial flaw plane. As noted previously, this multiple cracking appears to be characteristic of the biaxially loaded specimens.

The cruciform specimen data developed for heat-treated plate 14 is shown in Table 2.1 and Fig. 2.11. Trend lines have been included to aid in an interpretation of the data.

ORNL-DWG 97-142019 EFG

Figure 2.7 Fracture surface of cruciform specimen P6A. Location of apparent initiation site is near centerline of specimen.

Figure 2.8 CMOD for failure test of cruciform specimen P10A, $P_T/P_L = 0.6:1$, $T_f = 8^\circ\text{C}$ (46°F)

Figure 2.9 LLD for failure test of cruciform specimen P10A, $P_T/P_L = 0.6:1$, $T_f = 8^\circ\text{C}$ (46°F)

ORNL-DWG 97-142022 EFG

Figure 2.10 Fracture surface of cruciform specimen P10A. Location of apparent initiation site is near centerline of specimen, although several secondary sites are present.

ORNL-DWG 97-142023 EFG

Figure 2.11 Cruciform specimen data from heat-treated plate 14

Constraint

The low-temperature end of the curves was established using 1/2T compact data adjusted to full constraint using the Irwin β estimating procedure. Figure 2.11 shows the effect of increasing both flaw-tip constraint and test temperature on the fracture toughness of this material. Under full biaxial (1:1) loading, the toughness values continue to be lower than those values for uniaxial (0:1) and biaxial (0.6:1) loadings for all three test temperatures investigated. Testing of additional biaxial specimens at higher temperatures will be performed to establish the shape of the shallow-flaw fracture-toughness curve through the lower transition temperature region. The current plan is to continue to test specimens in pairs, one 0.6:1 and one 1:1, at the same temperature. No additional uniaxial tests are planned for temperatures greater than -5°C (22°F).

During this period, new η -factor analyses were performed, and revised toughness values were calculated for the specimens tested at -5°C (22°F) and above. In making comparisons between the calculated and measured deformation responses of the cruciform specimens, it was observed that the specimens usually showed lower plastic strain hardening than was predicted by the analyses. The measured stress-strain curve for the plate 14 material was adjusted slightly to decrease hardening to provide a better match between the observed and calculated deformation results.

The new η -factors calculated in this manner resulted in only very small changes in toughness. The revised toughness values are those shown in Table 2.1.

2.2 Biaxial-Loading Effects on Constraint (W. J. McAfee, B. R. Bass, and J. W. Bryson)

2.2.1 Plate 14 Cruciform Results

The plate 14 cruciform results were evaluated in an attempt to deduce the role of constraint on the toughness values being obtained. Figure 2.12 shows all the heat-treated plate 14 data plotted vs normalized temperature ($T - \text{NDT}$). For comparison, all of the 1/2T compact data generated for this material are also shown. Those compact data plotted with a diamond symbol did not fail up to the limit of the extensometer and, though not considered valid, have been used to determine trends. The 1/2T data on the lower shelf were adjusted to full plane-strain conditions using the Irwin β_{IC} specimen size adjustment as modified by Merkle.² These results were then used as an "anchor point" for the trend lines through the cruciform data. Although the test temperatures for the data in Fig. 2.12 are $\leq \text{NDT}$, the

Figure 2.12 Plate 14 cruciform data showing effect of increasing flaw-tip constraint (biaxial loading) on fracture toughness in lower transition temperature range. Data are compared to low-constraint 1/2T compact specimens.

cleavage behavior of the heat-treated plate 14 material does not correlate well with data for other materials or with the ASME Sect. XI lower bound curve (see Fig. 2.11) when using the measured value of NDT, T_{CV} , or RT_{NDT} . For example, the midtransition temperature range appears to be $\sim 50^\circ\text{C}$ below NDT ($+40^\circ\text{C}$), as shown in Fig. 2.12. These questions then arise: (1) Is a 4T specimen with $a/W = 0.5$ more highly constrained than the cruciform under biaxial (1:1) loading? (2) Do conventional temperature normalizing parameters not provide a reliable normalizing procedure for this material? These observations make it difficult to evaluate the degree of constraint in these specimens when looking at these data alone. As more data are generated and a better understanding of these roles of constraint in determining fracture toughness are developed, it may be possible to resolve the questions.

2.2.2 Analytical Investigation of Constraint Effects

To investigate constraint in the cruciform specimens further, several different analyses were performed to study the relationship between estimated toughness K_J and plastic zone size R_{pl} . The analyses were performed using the Combustion Engineering (CE) material properties because, at this time, a larger base of plastic zone size calculations exist for this material than for the plate 14 material.^{3,4} The present ORNL $K_J - \ln(R_{pl})$ dual-parameter constraint model uses a proportional limit stress (0% plastic strain)

definition to determine R_{pl} . The use of the proportional limit stress was initially selected because it gave $K_J - \ln(R_{pl})$ contours that were better defined than those using yield stress, and strain beyond proportional limit is more representative of the total plastic strain developed. In previous analyses for the CE material, constraint-dependent $K_J - \ln(R_{pl})$ trajectories were developed using a five-piece multilinear material representation with plasticity initiating at the proportional limit and terminating in a perfectly flat Lüder's plateau. The CE as-received measured properties used in these prior analyses have a proportional limit of 379 MPa (55 ksi) and a yield stress (0.2% plastic strain) of ~ 400 MPa (58 ksi). Because yield stress is more conventionally used in elastic-plastic FEA fracture analyses, the purpose of this first set of analyses was to investigate the impact on the $K_J - \ln(R_{pl})$ relationship when different measures of the onset of plasticity were used.

Figures 2.13–2.16 show $K_J - \ln(R_{pl})$ trajectories for 0, 0.2, 1, and 5% plastic strain, respectively. Contours of equivalent plastic strain (strain-based) rather than their stress equivalents were used to construct the trajectories. Only uniaxial (0:1) and biaxial (1:1) loadings were considered, with the low-toughness biaxial (1:1) result (BB10) plotted for reference. The 0% plastic strain, Fig. 2.13, gives constraint-dependent trajectories that basically repeat prior results. A 0.2% plastic strain, Fig. 2.14, definition shows some constraint dependence at higher loads, but not at the BB10 failure load. At 1%, Fig. 2.15, and 5%, Fig. 2.16,

Figure 2.13 $K_J - \ln(R_{pl})$ trajectories using proportional limit (equivalent plastic strain = 0%) to determine R_{pl}

Figure 2.14 $K_J - \ln(R_{pl})$ trajectories using conventional yield strain (equivalent plastic strain of 0.2%) to determine R_{pl}

Figure 2.15 $K_J - \ln(R_{pl})$ trajectories using equivalent plastic strain of 1% to determine R_{pl}

Figure 2.16 $K_J - \ln(R_{pl})$ trajectories using equivalent plastic strain of 5% to determine R_{pl}

strain levels, the $K_J - \ln(R_{pl})$ model shows essentially no constraint dependence. It can be concluded that all non-linear strain must be included for good $K_J - \ln(R_{pl})$ correlation. Use of the yield point, as would be done in most analyses, will lead to prediction of reduced effects of biaxial loading on constraint. Were the analyses performed using a high yield point material, potentially no biaxial effect at all would be predicted.

Analyses were also performed to investigate applicability of the $K_J - \ln(R_{pl})$ dual-parameter model to a range of flaw and specimen configurations. In the comparison and interpretation of these analysis results, the size of the plastic zone is used as a qualitative measure of the constraint around the flaw tip. Small strain analyses were performed on block models with a 51-mm (2.0-in.) deep ($a/W = 0.5$) straight-edge flaw. Two models were evaluated, a $102 \times 102 \times 102$ -mm ($4 \times 4 \times 4$ -in.) block and a $102 \times 254 \times 102$ -mm ($4 \times 10 \times 4$ -in.) block. The 254-mm-wide block was included for comparison to observe the magnitude of additional constraint introduced by an increase in specimen width (increase in flaw length with the same flaw depth). The models were loaded in uniaxial 0:1 tension, biaxial 1:1 tension, and biaxial 2:1 tension. For comparability with the previous analyses, material properties for the CE material in the as-received condition were used for reference. Tension loading was selected for convenience because a smaller, less complex model could be used. The crack-tip region of the specimen, however, was modeled with

the identical mesh refinement as is used in the two-dimensional (2-D) flaw cruciform beam analyses. The model was loaded until the crack-tip plastic zone just reached the loaded back-surface; that is, there are no "free surface effects" during the loading history. Both of these deep-flaw models represent high constraint conditions for which no shallow-flaw effects (loss of constraint) would be expected.

The analyses results are shown in Fig. 2.17. For the 102-mm-square block loaded under plane strain, 0:1 uniaxial, and 1:1 biaxial conditions, the $K_J - \ln(R_{pl})$ trajectories show that (1) at low loads there is no distinction between the three loading conditions; (2) at loads for which the crack-tip plastic zone begins to approach the back and bottom (face opposite flaw) faces of the specimen, there is a loss of constraint in the uniaxially loaded case as indicated by an increase in R_{pl} for uniaxial loading as compared to plane-strain conditions; and (3) application of biaxial loading restores a constraint condition equivalent to plane strain. These results demonstrate that the dual-parameter model reduces to a single-parameter model for conditions of high constraint, that is, the analyses yield essentially the same contour for high constraint. For the 254-mm-wide specimen loaded uniaxially, the constraint is greater than that for the 102-mm-wide specimen but less than for plane-strain conditions. This difference indicates the importance of specimen width (for uniaxially loaded specimens) in obtaining valid fracture-toughness values.

Figure 2.17 $K_J - \ln(R_{pl})$ trajectories for a $102 \times 102 \times 102$ mm specimen with deep flaw ($a/W = 0.5$). CE material as received, small strain analysis, tension loading.

A further important loading condition is that for pressure loading in an RPV with a circumferential flaw. For this case, the flaw is subjected to 2:1 biaxial loading. The results for the 2:1 load case using both models are also shown in Fig. 2.17. Both these cases result in higher constraint than for the plane-strain condition; the wider specimen is more constrained than the square specimen. These results immediately raise the question, "Do plane-strain conditions represent a true fracture-toughness lower bound?" This question cannot be answered analytically, but the resolution can be obtained using specimens in the biaxial-loading matrix currently being tested. The 2:1 biaxial loading can be obtained by proper configuration of the biaxial test facility with no alterations to specimen or test fixture design.

To investigate this further, analyses were performed to evaluate the potential behavior of the heat-treated plate 14 specimens to flaw geometry and loading. Figure 2.18 shows a comparison of the resulting constraint, as measured by R_{pl} , for shallow-flaw (0:1, 0.6:1, and 1:1) and deep-flaw plane-strain behavior. At low load levels, the deep flaw under plane-strain conditions (high constraint) has approximately the same constraint as does the shallow-flaw 0.6:1 biaxial loading. At higher loads, the 0.6:1 biaxial loading shows a rapid increase in R_{pl} . This phenomenon is interpreted as a loss of constraint. Shallow-

flaw uniaxial (0:1) loading shows less constraint over the full load range, and shallow-flaw biaxial (1:1) loading shows more constraint at low loads, transitioning to less constraint at higher loads. Figure 2.19 shows a comparison of results for shallow-flaw heat-treated plate 14 behavior for load ratios of 0:1, 0.6:1, 1:1, and 2:1. At very low loads, the 2:1 biaxial-load case shows the highest level of constraint but then undergoes a rapid expansion of R_{pl} to a condition with less constraint than the uniaxial (0:1) loading case. With a further increase in load, all four R_{pl} trajectories converge, thus indicating a loss of constraint for all biaxial-load ratios at high loads for shallow flaws. These analysis results are intended primarily for comparative purposes. They do point out that an interpretation of constraint based on a measure of the plastic strain developed around the flaw tip shows that a loading condition may exist and will result in constraint greater than what would be determined for uniaxially loaded, deep-flaw specimens. This issue of whether plane-strain conditions represent a true fracture-toughness lower bound should be further explored because its resolution may directly impact the definition of flaw depth and load ratio lower bound(s) fracture-toughness curve for use in RPV evaluations. Testing of plate 14 cruciform specimens under biaxial load ratios > 1 would provide insight into this problem. Although no specimens are currently specific for such tests, limited material is available, and specimens could be fabricated to address this issue.

Figure 2.18 $K_J - \ln(R_{pl})$ trajectories for shallow- and deep-flaw cruciform specimen under uniaxial or biaxial bending. Heat-treated plate 14 material, small strain analysis.

Figure 2.19 $K_J - \ln(R_{pl})$ trajectories for shallow-flaw cruciform specimen under uniaxial or biaxial bending. Heat-treated plate 14 material, small strain analysis.

References

1. W. J. McAfee, Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Material Characterization," in "Heavy-Section Steel Technology Program Semiannual Progress Report for October 1995-March 1996," NUREG/CR-4219 (ORNL/TM-9593/V12&N2), Vol. 12, No. 2, 1997.*
2. J. G. Merkle, Union Carbide Corp. Nucl. Div., Oak Ridge National Laboratory, "An Examination of the Size Effects and Data Scatter Observed in Small-Specimen Cleavage Fracture Toughness Testing," NUREG/CR-3672 (ORNL/TM-9088), April 1984.*
3. J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Task 7: Integration and Results into a State-of-the-Art Methodology," "Heavy-Section Steel Technology (HSST) Program, Monthly Management Report for January 1994," ORNL/HSST/MME-94-4, February 1994.
4. W. J. McAfee, Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Biaxial Loading Effects," in "Heavy-Section Steel Technology Program Semiannual Progress Report for October 1995-March 1996," NUREG/CR-4219 (ORNL/TM-9593/V12&N2), Vol. 12, No. 2, 1997.*

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

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 Clad Cruciform Beam Testing

Program (B. R. Bass, W. J. McAfee, and J. W. Bryson)

3.2.1 Cruciform Beam Specimen

The HSST Program is engaged in fracture technology development that addresses specific issues related to structural integrity assessments of RPVs subjected to PTS and P-T loading transients. Matrices of uniaxial and cruciform beam tests were defined to investigate and quantify effects of the clad structure on fracture initiation toughness of 2-D (infinite-length) shallow flaws and 3-D (finite-length) through-clad, shallow, surface flaws (Table 3.1). Shallow-flaw fracture toughness of RPV material has been shown to be higher than that for deep flaws, because of the relaxation of crack-tip constraint.¹ Tests of full-thickness clad beams [single-edged, notched beam (SENB) specimens] are used to quantify this shallow-flaw effect in specimens that are prototypic of RPV wall thickness and material properties.^{2,3} However, the SENB specimens do not address biaxial-loading effects produced in the RPV pressure boundary by transient thermal and pressure loading. A typical biaxial stress field is illustrated in Fig. 3.1(a), together with a shallow through-clad surface flaw. One of the principal stresses in Fig. 3.1(a) is seen to be aligned parallel to the crack front of the shallow flaw. The cruciform specimen [Fig. 3.1(b)] was developed at ORNL to introduce a prototypic, far-field, out-of-plane biaxial stress component that provides a linear approximation of the nonlinear stress distribution, as shown in Fig. 3.1(a) (Refs. 4–6). This out-of-plane component has the potential to increase constraint in the region of the crack-tip plastic zone and thereby reduce the shallow-flaw fracture-toughness enhancement.⁷

Biaxially loaded cruciform beam specimens are being used to investigate the behavior of shallow, finite-length, through-clad surface flaws where the cladding, temperature, and biaxial-load ratio are the primary independent

variables. The specimens to be tested range from medium-size beams ($102 \times 102 \times 102$ mm test sections) to large-size beams ($154 \times 154 \times 154$ mm test sections). The specimens will be tested under biaxial-load ratios ranging from 0.0 (uniaxial) to 1.0 (full biaxial). (The biaxial-load ratio is defined as P_T/P_L , where P_T is the total load applied to the transverse beam arms and P_L is the total load applied to the longitudinal arms.) The test matrix also includes unclad cruciform specimens (containing 2-D flaws) fabricated from plate material that was heat-treated to produce a high yield strength prototypic of an irradiated RPV (Ref. 8). Data from the clad cruciform tests are intended to help define an envelope of flaw sizes for which cleavage initiation would not be expected under severe PTS-type loading. Development of such an envelope will allow exclusion of a family of small flaws from probabilistic analyses, potentially contributing to a reduction in the calculated conditional probability of failure in RPV integrity assessments.

The same RPV shell-source material as that used for the full-thickness, clad-beam specimens is being used to fabricate clad cruciform specimens with a semielliptic surface flaw. The specimens are being machined from the inner half of the shell thickness, as is shown schematically in Fig. 3.2. Load-diffusion-control slots are machined into the specimen loading arms to create the boundary conditions required to achieve a uniform biaxial stress field in the central test section of the specimen. After fatigue pre-cracking, the flaws described herein have depth (a) and surface length (2c) dimensions of 19 and 53 mm, respectively. The test flaw is oriented with its major axis parallel to the direction of deposition of the structural weld. The leading edge of the test flaw passes through the clad material, the clad HAZ, and into the shell fabrication weld material. For each cruciform specimen, fatigue sharpening of the flaw is completed prior to the attachment of the transverse loading arms by the EB welding process. Measured temperature data taken during EB welding of the transverse arms imply that temperatures produced in the cruciform test section ($<150^\circ\text{C}$), are not high enough to initiate processes that would reduce fracture toughness, such as locally intensified strain-aging (LISA) embrittlement.⁹ Steps in the fabrication process are depicted in the photographs of Fig. 3.3. The specimen design is capable of reproducing a linear approximation of the nonlinear biaxial stress distribution shown in Fig. 3.1(a). This design, coupled with a statically determinate load-reaction system, permits the specimen to be loaded in either uniaxial (4-point bending) or biaxial (8-point bending) configurations. Tests of nominally

Table 3.1 Matrices of unclad and clad beam tests defined to validate constraint methodologies

HSST task	Specimen configuration	Specimen size	Flaw type	Crack geometry: depth/thickness (a/W) length/depth (2c/a)	Material	Number of tests	
						Planned	Completed
H3	Uniaxial (SENB)	Full RPV shell thickness	2-D, shallow and deep	$0.05 \leq a/W \leq 0.5$ (uniform depth)	RPV structural weld with clad layer	5	5
H3	Uniaxial (SENB)	Full RPV shell thickness	2-D, shallow	$0.05 \leq a/W \leq 0.10$ (uniform depth)	RPV base material with clad layer	3	3
H3	Cruciform	Medium (102-mm-thick test section)	Finite-length, shallow	$0.05 \leq a/W \leq 0.25$ $2c/a = 2.8$ (semielliptic)	RPV weld with clad layer	24	8
H3	Cruciform	Large (154-mm-thick test section)	Finite-length, shallow	$0.05 \leq a/W \leq 0.25$ $2c/a = 2.8$ (semielliptic)	RPV weld with clad layer	4	0
H2	Cruciform	Medium (102-mm-thick test section)	2-D, shallow	0.1 (uniform depth)	Heat-treated ^a A 533 B plate (HSST plate 14)	24	22
H2	Cruciform	Medium (102-mm-thick test section)	2-D, shallow	$0.05 \leq a/W \leq 0.25$ (uniform depth)	Heat-treated ^a A 533 B plate (HSST plate 14)	8	0
H2	Cruciform	Large (154-mm-thick test section)	2-D, shallow	0.1 (uniform depth)	Heat-treated ^a A 533 B plate (HSST plate 14)	4	0

^aHeat treated to simulate irradiated material properties.

ORNL-DWG 97-142032 EFG

Figure 3.1 Cruciform shallow-flaw fracture toughness specimen approximates biaxial stresses produced by PTS loading in an RPV wall

ORNL-DWG 97-142033 EFG

Figure 3.2 Finite-length flaw specimens cut from an RPV shell segment are used to investigate the effects of the clad layer and flaw size on initiation behavior of prototypic RPV welds

Figure 3.3 Clad cruciform specimens are fabricated from either a machined test section or a uniaxial beam. After fatigue precracking, transverse beam arms are EB-welded to form a cruciform specimen.

identical specimens can be performed with the level of stress biaxiality as the only test variable.

3.2.2 Specimen Validation

Development tests of the shallow-flaw cruciform beam specimen were performed to validate the specimen design and to confirm the required test procedures. It was necessary to carefully control the geometry of the test flaws so that direct comparisons of apparent toughness and initiation-site location could be made for specimens tested under different biaxial-loading conditions or temperatures. Specimens machined from homogeneous plate material, that is, no cladding, were used to demonstrate that the machined starter flaw geometry could be fatigue-precracked into a flaw of the required final geometry for testing. Extensive temperature control trials were conducted to verify the performance of the temperature monitoring and control systems. Control of the test temperature was achieved using various liquid nitrogen distribution systems. Instrumentation applied to the test specimens included thermocouples, strain gages, clip gages, and displacement transducers. Both CMOD and LLDs were monitored continuously throughout each of the tests.

3.2.3 Testing of Cruciform Clad Specimens

Thus far, eight specimens in the HSST matrix of cruciform clad beam experiments have been tested under conditions that are summarized in Table 3.2. Three of the specimens had the clad layer (~5 mm thick) carefully removed from the test section before flawing and welding of the beam arms; these are denoted in Table 3.2 as a HAZ-type specimen. The cladding was left intact on the remaining five cruciform beams (defined as CLAD-type specimens in Table 3.2). The two types of specimens were identical for all other parameters. Testing of HAZ- and CLAD-type

specimens containing the same surface flaw geometry permits an evaluation of cladding effects on surface-flaw behavior. The effects of biaxial-load ratio and material composition on cleavage initiation behavior are of primary interest (see Fig. 3.4).

Calculations of fracture-toughness values from the measured data (described later) are dependent upon location of the cleavage initiation site(s) relative to the locus of the fatigued flaw front. Usually, identifying these sites in the cruciform clad specimens requires careful study of the fracture surface. Occasionally, the initiation site is readily apparent in the fracture surface, as shown in Fig. 3.5 for a cruciform beam tested under a biaxial-loading ratio of 1.0. Initiation sites were determined for the eight specimens (as given in Table 3.2) from fractographic examination of the fracture surfaces; these sites are depicted schematically in Fig. 3.6.

3.2.4 Analysis of Cruciform Clad Beams

Three-dimensional finite-element models were prepared for both the CLAD- and HAZ-type specimens. Figure 3.7(a) shows an overview of a quarter section of the model used for both specimens, and Fig. 3.7(b) shows details of the flaw geometry and material distribution. Both specimen types were modeled with a 19-mm-deep by 53-mm-long semielliptical surface flaw located in all-weld-metal. For the CLAD-type specimen, the clad layer is 5 mm thick and is represented by the top two layers of elements, which are shown in Fig. 3.7(b). The next two layers represent a 5-mm-thick HAZ. For the HAZ-type specimen, the top two layers are HAZ material, and the next two layers are weld metal. In both models, the same flaw geometry is embedded in a slab region of weld metal with a thickness of 25.4 mm (12.7 mm in finite-element model due to symmetry) as measured in the direction of the longitudinal beam arms.

Table 3.2 Summary of cruciform clad beam test results

Test specimen	Test temperature (°C)	Biaxial ratio P_T/P_L	T - NDT (°C)	K_{Jc} (MPa $\sqrt{m}$)
CLAD-1	-56.7	0.0	2.8	162.6
HAZ-1	-57.8	0.0	1.7	108.4
CLAD-2	-20.6	1.0	38.9	328.0
CLAD-3	-25.0	1.0	34.4	143.8
CLAD-4	-37.2	1.0	22.2	148.7
HAZ-2	-40.0	1.0	19.4	150.2
CLAD-5	-56.1	1.0	3.3	67.1
HAZ-3	-61.1	1.0	-1.7	75.6

Elastic-plastic fracture analyses of both models were performed using a material representation determined from tensile tests. The analysis procedures require that close agreement be obtained between measured and calculated load vs CMOD response of the specimen. Usually, this agreement was achieved by making minor adjustments to the input stress-strain curves. For a given flaw geometry, the FEAs provide a relationship between K_J and location along the flaw front expressed as an angle measured from the free surface, with applied load as a parameter. Test results provide the failure load and location of the initiation site on the flaw front for determination of fracture toughness from the computational analyses (Fig. 3.8).

Figure 3.4 Tests of clad cruciform beams with shallow, finite-length surface flaws are used to characterize the effects of biaxial loading ratio and material composition cleavage initiation behavior in RPV materials

ORNL-DWG 97-142035 EFG

Figure 3.5 Unique cleavage initiation site for clad cruciform beam tested under uniaxial loading

ORNL-DWG 97-142036 EFG

Figure 3.6 Summary of location of cleavage initiation sites for clad cruciform specimen tests. Open symbols identify initiation site locations on the semielliptical flaw front.

Figure 3.7 Finite-element model used to analyze cruciform beam specimens: (a) quarter section; (b) detail of flaw geometry

Figure 3.8 Schematic showing relationship between circular angle and location of cleavage initiation site on semielliptical flaw boundary

3.2.5 Biaxial Loading and Cleavage Fracture Toughness

Figures 3.9 and 3.10 show the variation of K_I along the crack front for the CLAD-type specimen geometry for uniaxial (0.0) loading and biaxial (1.0) loading, respectively; Figs. 3.11 and 3.12 show K_I distributions for the HAZ-type specimen geometry for uniaxial (0.0) loading and biaxial (1.0) loading, respectively. In the CLAD-type specimen geometry, a four-step analysis was used that accounts for clad residual stress: (1) uniform cooldown of an *uncracked* specimen to introduce a previously measured 138-MPa clad residual stress, (2) release of fixities normal to the crack face to simulate introduction of the flaw via end saw-cutting, (3) uniform cooldown of cracked specimen from room temperature to test temperature of -57°C , and (4) application of mechanical loading. For the HAZ-type specimens, the first two steps were omitted because removal of the clad layer would have released essentially all of the residual stress associated with differential expansion of the clad-base metal structure in cooling from the stress-free temperature to room temperature.

The analytical results in Figs. 3.9–3.12 demonstrate a propensity for both the CLAD- and HAZ-type specimen geometries to initiate at the deepest point (90°) for uniaxial

(0.0) loading. Some of the CLAD- and HAZ-type beam fracture-toughness results (based on failure loads from Table 3.2 and initiation sites from Fig. 3.6) are also shown for comparison in Figs. 3.9–3.12. For biaxial (1.0) loading, the CLAD-type analytical models show a roughly equal likelihood of initiating in the HAZ or at the deepest point; the HAZ-type models show a slight disposition toward initiating in the HAZ layer. The fractographic data in Fig. 3.6 indicate that the uniaxially loaded CLAD-1 specimen ($T - RT_{NDT} = 3^\circ\text{C}$) failed near the deepest point, and the biaxial (1.0) loaded CLAD-5 specimen tested at $T - RT_{NDT} = 3^\circ\text{C}$ failed near the HAZ-weld interface. However, the remaining CLAD-type beams tested under biaxial loading at higher temperatures tended to fail toward the deepest point of the flaw. The two biaxially loaded HAZ-type specimens initiated at the HAZ-weld interface (Fig. 3.6). These observed failure locations are possible evidence of a constraint effect and/or variability of toughness between the weld and HAZ materials that is temperature-dependent, based on the very limited data compiled thus far. It is anticipated that, as more data are generated, the effects of constraint and material variability on the clad specimens can be quantified.

All the CLAD- and HAZ-type beam data are shown in Fig. 3.13 and are plotted as a function of normalized test

Figure 3.9 Calculated SIF vs angle along flaw front and applied moment loading for clad-beam specimen under uniaxial (0.0) loading

Figure 3.10 Calculated SIF vs angle along flaw front and applied moment loading for clad-beam specimen under biaxial (1.0) loading

Figure 3.11 Calculated SIF, K_J , vs angle along flaw front and applied moment loading for HAZ beam specimen under uniaxial (0.0) loading

Figure 3.12 Calculated SIF, K_J , vs angle along flaw front and applied moment loading for HAZ beam specimen under biaxial (1.0) loading

Figure 3.13 Comparison of clad and HAZ cruciform beam data with ASME Sect. XI K_{Ic} curve

Evaluation

temperature. Some general observations can be made concerning this very limited data set. First, biaxial loading of both CLAD- and HAZ-type specimens tends to result in a lower toughness regardless of the location of the initiation site. This effect is more pronounced for the CLAD-type as compared to the HAZ-type specimens. Second, the weld material is susceptible to ductile tearing at temperatures greater than $T - RT_{NDT} = 30^{\circ}\text{C}$. This basically establishes an upper limit on test temperature for this series of specimens, if precleavage ductile tearing is to be minimized. Finally, the lowest toughness value, obtained for the CLAD-type beam specimen under biaxial loading, is only slightly above the ASME Sect. XI curve.

In Fig. 3.14, fracture-toughness values for the four cruciform specimens from Table 3.2 tested in the temperature range ($T - RT_{NDT}$) from -2 to $+3^{\circ}\text{C}$ are plotted as a function of biaxiality ratio. In the same figure, fracture-toughness values are shown for cruciform specimens fabricated from a heat-treated HSST plate 14 material and tested at three biaxiality ratios near $T - RT_{NDT} = -43^{\circ}\text{C}$ (Ref. 8). As noted previously, the latter (unclad) material was heat-treated to produce high-yield strength values associated with irradiated RPV materials. For all biaxiality ratios, the results in Fig. 3.14 imply that shallow-flaw

fracture-toughness data from the RPV structural weld material are significantly lower than those obtained from the high-yield strength plate material. Furthermore, biaxial loading is effective in reducing the fracture toughness of the high-toughness plate material, as well as the relatively low-toughness clad/HAZ/structural-weld region of the RPV shell.

3.3 Role of Residual Stresses in Clad Finite-Length Flaw (W. J. McAfee and J. W. Bryson)

Previous analyses have shown that the presence of residual stresses in the full-thickness clad beams has a distinct influence on the critical fracture toughness determined from test results. An analytical and experimental study was thus performed to quantify, as best possible, the probable magnitude of residual stresses in the clad layer and how best to incorporate residual stress effects into toughness determinations for the clad cruciform beams. The experimental effort was very straightforward. Two full-thickness blocks were saw-cut from the clad shell. Each block was next machined to a parallelepiped 102 mm long $\times$ 25 mm wide $\times$ 235 mm thick (shell thickness) ($4 \times 1 \times 9 \frac{1}{4}$ in.),

Figure 3.14 Biaxial tests in (a) heat-treated A 533 B plate material with constant-depth, shallow surface flaws and (b) prototypic RPV weld material with finite-length semielliptical surface flaws show a consistent trend of decreasing fracture toughness with increasing load-biaxiality ratio

as shown in Fig. 3.15. One block was taken with the length in the circumferential direction of the shell (parallel to the weld deposition direction) and the other in the axial direction (transverse to the weld deposition direction). As part of the machining process, fiducial marks were applied along the clad surface and on each end of the parts. Precision dimensional inspections were performed at this point to measure the x, y, and z coordinates of each of these fiducial marks. The clad layer was then machined away from the base material by sawing ~10 mm below the clad surface. The remaining base material was then carefully machined away using cuts ~0.13 mm (0.005 in.) deep until the center of the clad-base metal fusion zone was revealed. Visual inspection was used to judge when equal amounts of base and clad materials were exposed on the machined surface. The clad layer parts were reinspected to measure the distorted shape as compared to the original shape. For elastic behavior and a modulus of 206 GPa (29,900 ksi), the deformation results were used to calculate the average membrane component of circumferential residual stress [143.1 MPa (20.75 ksi)] and axial residual stress [51.0 MPa

(7.4 ksi)] within the clad layer. These values were subsequently used in evaluating the potential impact of clad residual stresses on the calculated K_I for the clad cruciform beams.

Imposition of a preexisting internal, residual stress distribution within a solid body as initial conditions for a 3-D FEA poses severe modeling problems. Ordinarily, residual stresses are imposed through the use of a through-thickness thermal gradient. This was the approach used here. The problem was first considered from the standpoint of the full-size vessel and the method for introducing the residual stresses. After the clad layer has been deposited, cooldown to room temperature from the postweld heat-treatment (PWHT) temperature [621°C (1150°F)] creates large tensile stresses in the cladding and small compressive stresses in the underlying shell material. Figure 3.16 shows a segment of a 2-D finite-element model for an uncracked RPV having $R_i = 218$ cm (86.0 in.) and $T = 22.9$ cm (9.0 in.) that was used to evaluate the residual stress problem. The

ORNL-DWG 97-142045 EFG

Figure 3.15 Schematic of inspection blocks used to measure clad layer distortion caused by relaxation of residual stresses

Figure 3.16 Finite-element model for clad residual stress investigation

analysis was performed in three steps: (1) cooldown of the complete RPV, (2) removal of material and release of boundaries to leave the cruciform beam, and (3) introduction of the test flaw. In Fig. 3.16, the beam model is actually embedded within the RPV wall model. For step 1, the RPV was uniformly cooled from 621°C (1150°F) to 22°C (72°F). For step 2, the element death option in ABAQUS¹⁰ was used to simulate saw cuts to the final 102 × 102 × 102 mm (4 × 4 × 4 in.) dimensions. The 19-mm (0.75-in.) deep test flaw was then modeled by releasing nodal constraints on the crack face and allowing the crack to open under the residual stress field from step 2. Properties reported in Keeney¹¹ were used for base, weld, and clad materials.

Fig. 3.17 shows opening-mode stress distributions for each of the three steps in the analysis. Cooldown from PWHT produces ~241 MPa (35 ksi) stress in the cladding. Cladding stresses are lowered ~68.9 (10 ksi) by saw cuts to final dimensions. This finding agrees well with the residual stress 145 MPa (21 ksi) measured experimentally. Introduction of the flaw by releasing crack face nodal fixities produces $K_I \sim 4.4 \text{ MPa} \sqrt{\text{m}}$ (4 ksi $\sqrt{\text{in.}}$), a relatively small effect.

The residual stresses in the specimen weld also may exert an influence on the flaw K_{IC} values. Two analysis methods were used to evaluate the magnitude of residual stresses in the clad cruciform beam specimens. Keeney¹¹ used a thermal gradient method (TGM) to estimate the residual stresses in the weld of full-section, 229-mm (9-in.) clad beams tested at the National Institute of Standards and Technology (NIST). Although this technique underpredicts clad residual stress, it is useful for estimating residual stress in the weld. The TGM used a temperature distribution in the form of a modified cosine function to generate stresses in the beam that matched the calculated crack opening with the crack opening from laboratory measurements. Following the methodology reported by Keeney,¹¹ a 2-D finite-element model for a 229-mm-deep (9-in.) beam and the TGM were used to generate the stress distribution in the weld. To simulate the weld stress in a 102-mm-deep (4-in.) beam, the resultant temperature distribution was truncated at a depth of 102 mm (4 in.), and this truncated distribution was used to calculate the stresses in the cruciform specimen. The second technique considered was the death-element procedure discussed previously. That is, from the full-thickness beam model with the thermal gradient applied, all elements not representing the clad cruciform were "killed," resulting in stress

Figure 3.17 Opening mode stress distributions in 3-step 2-D analysis to determine residual stress in clad layer for clad cruciform specimens

redistribution within the new model. Figure 3.18 shows the resulting residual stress distributions in the 102-mm (4-in.) beam using both techniques, and it shows their equivalence. Finally, Fig. 3.19 gives weld residual stress distributions in both the 102- and 229-mm beams, respectively, showing that the residual stress in the 102-mm beam over the 19-mm flaw section is reduced from ~83 to 14 MPa. Hence, machining of the clad cruciform specimens from the full shell thickness essentially removes weld residual stresses.

These results show that residual stresses play a relatively insignificant role in the development of finite-length flaw K_I values in the 102-mm-deep (4-in.) clad cruciform beams. Very little error would be introduced by ignoring them altogether. To be consistent, the effects of residual stress were included, however, in the 3-D clad finite-length flaw calculations to be used in toughness data reduction by performing a sequential four-step analysis: (1) uniform cooldown of an *uncracked* 102 × 102 × 102 mm specimen from a stress-free temperature to 22°C; this reproduces the measured 145-MPa stress in the clad layer; (2) release of nodal fixities on the crack face to simulate introduction of the finite-length flaw (i.e., create and allow the crack to open under the stress field from step 1 and produce an initial K_I); (3) uniform cooling from 22°C to the test temperature; and (4) application of mechanical loads.

Analyses have been performed for the finite-length flaw (53.3- by 19-mm) clad cruciform specimen under uniaxial (0:1) and biaxial (1:1) loading. Residual stresses were considered as discussed previously. It was determined that an initial stress-free temperature of 400°C gave 145-MPa room-temperature residual stress in the clad. Tensile test characterization data for clad, weld, HAZ, and base material were used.

For the uniaxial load case, the analysis became unstable beyond 1588 kN (357 kips) and was discontinued. The K_I -distributions along the crack front are shown in Fig. 3.20 and indicate that failures near the maximum depth location would be expected. These analyses still must be verified to ensure that the deformation predictions match the measured CMOD and LLD so that the toughness values determined directly from failure loads can be used with confidence.

Analysis results for the biaxial (1:1) loading case are shown in Fig. 3.21. The maximum SIF values occur in the HAZ between the clad and base materials. Fracture initiation behavior will then be highly dependent on the relative toughnesses of the component materials, specifically, HAZ and weld. Two clad cruciform tests (specimens 5-1 and 5-2) under 1:1 biaxial loading have been performed.

Figure 3.18 Residual stress distributions in 102-mm beam via (1) truncated temperature distribution and (2) element death

Figure 3.19 Weld residual stress distributions in 4- and 9-in.-deep beams via thermal gradient method (TGM)

Figure 3.20 Variation of K_I along crack front for finite-length flaw clad cruciform beam under uniaxial (0:1) loading

Figure 3.21 Variation of K_I along crack front for finite-length flaw clad cruciform beam under biaxial (1:1) loading

Evaluation

Interpolating from Fig. 3.21, the toughness values determined, considering fracture to have initiated in the HAZ, are 440 MPa $\sqrt{m}$ (specimen 5-1) and 360 MPa $\sqrt{m}$ (specimen 5-2). These high toughness values are consistent with the large deformation observed in tests of each specimen.

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

3.4.1 Drop-Weight Specimens

Drop-weight specimens were fabricated from both the circumferential and longitudinal welds of the RPV shell used as source material for the full-thickness and clad cruciform beam specimens. Specimens taken from the longitudinal weld immediately beneath the clad layer were oriented in the C-R direction to match previously tested Charpy specimens. The nil-ductility temperature (NDT) for the longitudinal weld was determined to be -59°C . The reference temperature (RT_{NDT}) of the longitudinal weld was taken as the larger of the values for $NDT = -59^{\circ}\text{C}$ and $(T_{CV} - 33.3\text{K}) = -61^{\circ}\text{C}$, (i.e., $RT_{NDT} = NDT = -59^{\circ}\text{C}$). This value for the longitudinal weld was used as the reference temperature for the clad cruciform tests because it was perceived to be more representative of the material used in the beam specimens.

3.4.2 Tensile Specimens

Three different specimen configurations were used to characterize the tensile properties of the various material regions of the clad cruciform beam specimens. These included standard round bar specimens and standard and modified sheet-type specimens. Examples of these specimens depicted in the photographs of Fig. 3.22 were all tested at -50°F (-46°C). Tensile data obtained from these specimens provide the stress-strain characterization of component materials required for determination of fracture toughness from the clad beam test data (Fig. 3.23).

Measurements were performed by UM and ORNL on the HAZ area of the RPV shell material. These data show that the HAZ area may have hardness numbers that are well above those for the clad layer and base material. Regions in which cladding was applied over base metal appear to result in the highest hardness values in the HAZ (Fig. 3.24).

3.5 Large-Scale Biaxial Test Fixture

During this report period, postinstallation shake-down testing of the new large-scale biaxial test fixture was completed at ORNL. The new 890-kN (200-kip) reactor pistons have a larger bore and stroke than the 534-kN (120-kip) pistons that they replace. The effect is that a larger volume of hydraulic fluid is displaced for the same piston movement, because a larger (oil) capacity divider piston is required. The selected piston has a slightly different piston area ratio than the one it replaced, 0.54 (new) vs 0.56 (old). This change will obviously have no effect for uniaxial and full biaxial (1:1) testing and very little effect for what has nominally been the 0.6:1 biaxial tests. The 0.6:1 ratio is taken from actual pressure measurements across the divider piston; therefore, the load ratio of the new piston can be easily verified during the first 0.6:1 failure test. Failure-initiation testing of clad cruciform beams using the new test fixture resumed during July 1996.

3.6 Analysis of Full-Thickness Clad Beams

Analytical studies continued at ORNL to characterize the Gurson-Tvergaard (G-T) plasticity model in the WARP-3D finite-element program¹² for application to the fabrication weld material in the full-thickness clad beams cut from an RPV shell section.^{2,3} Three-dimensional FEAs of data obtained from CT specimens fabricated from the weld material are being conducted to calibrate the G-T model parameters. When predictions from the G-T model match the experimental load vs LLD and J vs Δa curves from the CT specimens, the model will be applied to the full-thickness clad beam data obtained from test CB-1.5 (Ref. 3). These studies are conducted cooperatively with Task H.4 (ductile-to-cleavage mode conversion) and are described in detail in Chap. 4.

3.7 Development of Clad-Yielding Model

An important element in performing deterministic and probabilistic fracture analyses of RPVs subjected to transient loading conditions is the calculation of accurate SIFs for inner-surface cracks in clad (nonhomogeneous) cylinders. The linear-elastic fracture mechanics influence coefficient superposition methodology¹³⁻¹⁵ used by the FAVOR computer code¹⁶ provides considerable generality and economy in the generation of accurate SIF solutions.

Specimens Shown Were All Tested at -50°F (-46°C)

Base Material: Standard Round Base

Clad Material: Standard Sheet Specimen

HAZ Material: Standard Sheet Specimen

Weld Material: Modified Sheet Specimen

Figure 3.22 Three different specimen configurations used to characterize the tensile properties of the clad cruciform beam specimens

Figure 3.23 Stress-strain characterization of the component materials in the clad cruciform beam structure

ORNL-DWG 97-142053 EFG

Figure 3.24 Measurements of a prototypic RPV material show the HAZ area may have hardness numbers well above those for the clad layer and base material

Evaluation

This is a necessity for performing probabilistic fracture mechanics (PFM) analyses of RPVs. FAVOR SIF solutions may be obtained for a variety of flaw geometries and flaw depths without having to create time-consuming 2- and 3-D finite-element models. FAVOR SIF solutions may be generated for arbitrary stress distributions such as those produced by complex time-varying, thermal-hydraulic boundary conditions imposed on the inner surface of the RPV. The FAVOR SIF solutions have previously been validated¹⁷ by comparing them with those of ABAQUS;¹⁰ it is an NQA-1-certified, multidimensional, finite-element code with fracture mechanics capabilities.

For severe transient loading conditions, the RPV may be loaded beyond the clad or base material yield stress, thus introducing plasticity. It is suspected that ignoring plasticity introduces conservatism into the fracture analysis.

Currently, the FAVOR code is limited to performing linear-elastic stress analyses. FAVOR has a simplified optional method of including clad plasticity in a fracture analysis. It simply does not let the stress in clad region exceed a user-specified clad yield stress. The accuracy of this method is not really known. A current objective in this task is to develop and validate a computational model that more accurately approximates the effects of clad plasticity. This model must be able to be implemented into the FAVOR code.

During this reporting period, M. Beghini, a visiting Italian professor of material science from the University of Pisa, initiated work to develop a computational model to approximate the effects of cladding plasticity on the SIF for through-clad cracks subjected to PTS loading. Validation will be performed by comparing solutions generated by the approximate method with those generated by fully elastic-plastic ABAQUS solutions.

It was decided to start with a simple and idealized problem. A beam carrying a surface crack under pure bending loading was considered first. Both plane-stress and plane-strain conditions were applied. Dimensions and material properties were selected to produce conditions similar to those of a typical vessel wall. A through-thickness crack having a length 0.1 that of the beam width was assumed. To increase

the effect of plasticity, an elastic-plastic material with no strain hardening was selected.

Several SIF values were considered when applying the superposition principle:

- the direct elastic-plastic value obtained by using the nominal elastic-plastic stress applied to the crack faces,
- a pseudo-elastic nominal stress based on Neuber's approach (defined by equating the product of maximum stress and strain), and
- a pseudo-elastic nominal stress based on an approach proposed by Merkle (defined by equating the strain energy).

For this problem, plasticity tends to increase the SIF when compared to the elastic solution. Plasticity is effective before the nominal stress reaches the yield stress (Fig. 3.25) because the crack-tip plasticity is known to produce a virtual increase in the crack length. If the crack length is increased a proper quantity according to Irwin's model, good agreement was found between the superposition predictions using elastic nominal stresses and those generated by ABAQUS¹⁰ (Fig. 3.26).

For the plane-stress problem, the effect of plasticity at the crack tip is predominant. Plastic collapse is attained before a significant amount of nominal plastic strain has been produced in the specimen. In this case, the three approaches used in the superposition analysis give rather similar results (Fig. 3.26), but Neuber's curve is the closest to the ABAQUS solution.

The equivalent plane-strain problem has a reduced crack-tip plastic zone and a larger bending moment applied before reaching plastic collapse. In this case, both SIFs from elastic-plastic and Neuber's pseudo-elastic stress tend to underestimate the ABAQUS solutions, and Merkle's approach gives the best results (Fig. 3.27).

In the future, a crack cylinder will be analyzed under thermal shock loading, pressure loading, and a combination of pressure and thermal loadings. The effect of non-homogeneity will also be included by considering a layer of low-yield material at the inner surface.

Figure 3.25 Comparison of SIFs evaluated for the cracked beam in plane stress vs bending moment. Bending moment starts to produce plasticity in the equivalent uncracked problem at about 900 in.-kip. After $M = 1000$ in.-kip, the finite-element solution was not obtained due to nonconvergence. This effect resembles the physical condition of plastic collapse of the structure.

Figure 3.26 Crack length for WF analyses were increased by a quantity proportional to the crack-tip plastic zone to achieve the same value as the finite element solution at 900 in.-kip

Figure 3.27 Comparison for plane-strain conditions. Crack-tip plastic zone is smaller than in plane stress conditions, and the finite-element solution is more stable. After 1000 in.-kip, the crack is completely embedded in the nominal plastic zone. Merkle's approach seems to give the best results.

References

1. T. J. Theiss and D. K. M. Shum, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Experimental and Analytical Investigation of the Shallow-Flaw Effect in Reactor Pressure Vessels," NUREG/CR-5886 (ORNL/TM-12115), 1992.*
2. J. A. Keeney et al., Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Preliminary Assessment of the Fracture Behavior of Weld Material in Full-Thickness Clad Beams," NUREG/CR-6228 (ORNL/TM-12735), 1994.*
3. B. R. Bass et al., "Assessment of the Fracture Behavior of Weld Material from a Full-Thickness Clad RPV Shell Segment," pp. 299-311 in *Fatigue and Fracture Mechanics in Pressure Vessels and Piping*, PVP-Vol. 304 (American Society of Mechanical Engineers, 1995).[‡]
4. T. J. Theiss et al., Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Initial Results of the Influence of Biaxial Loading on Fracture Toughness," NUREG/CR-6036 (ORNL/TM-12349), 1993.*
5. B. R. Bass et al., Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Biaxial Loading and Shallow Flaw Effects on Crack-Tip Constraint and Fracture Toughness," NUREG/CR-6132 (ORNL/TM-12498), 1994.*
6. W. J. McAfee et al., Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Biaxial Loading Effects on Fracture Toughness of Reactor Pressure Vessel Steels," NUREG/CR-6273 (ORNL/TM-12866), 1995.*
7. W. E. Pennell et al., Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Preliminary Assessment of the Effects of Biaxial Loading on Reactor Pressure Vessel Structural Integrity Assessment Technology," ORNL/NRC/LTR-95/3, 1997.[†]
8. W. J. McAfee et al., "A Methodology for Assessment of Shallow-Flaw Fracture in Nuclear Reactor Pressure Vessels," to be presented at the 1997 American Society of Mechanical Engineers Pressure Vessel and Piping Conference, Orlando, Florida, 1997.

9. M. G. Dawes, "Significance of Locally Intensified Strain Aging to the Fracture Toughness of Welded Steel Structures," *Fracture Mechanics: 26th Volume, ASTM STP 1256*, W. G. Reuter, J. H. Underwood, and J. C. Newman, Jr., Eds. (American Society for Testing and Materials, Philadelphia, 1995).[‡]
10. *ABAQUS User Manual*, Hibbit, Karlsson & Sorenson, Inc., Providence, Rhode Island, 1995.
11. J. A. Keeney, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Residual Stress Analysis of Full-Thickness Clad Beam Specimens," ORNL/NRC/LTR-94/37, 1995.[†]
12. K. C. Koppenhoefer, A. S. Gullerud, C. Ruggieri, R. H. Dodds, Jr., and B. E. Healy, "WARP-3D: Dynamic Nonlinear Analysis of Solids using a Preconditioned Conjugate Gradient Software Architecture," UILU-ENG-94-2017, Dept. of Civil Engineering, University of Illinois at Urbana-Champaign, November 1994.
13. J. W. Bryson and T. L. Dickson, Martin Marietta Energy Systems Inc., Oak Ridge National Laboratory, "Stress Intensity Factor Influence Coefficients for Axial and Circumferential Flaws in Reactor Pressure Vessels," in *Pressure Vessel Integrity*, PVP-Vol. 250 (American Society of Mechanical Engineers, 1993), pp. 77-78.[†]
14. J. A. Keeney and T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Stress-Intensity-Factor Influence Coefficients for Axially Oriented Semielliptical Inner-Surface Flaws in Clad Vessels ($R_i/t=10$)," ORNL/NRC/LTR-93/33-Rev. 1, 1995.[†]
15. J. W. Bryson and T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Stress-Intensity-Factor Influence Coefficients for Circumferentially Oriented Semielliptical Inner-Surface Flaws in Clad Vessels ($R_i/t=10$)," ORNL/NRC/LTR-94/8, 1994.[†]
16. T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "FAVOR: A Fracture Analysis Code for Nuclear Reactor Pressure Vessels, Release 9401," ORNL/NRC/LTR 94-1, February 1994.[†]
17. T. L. Dickson, "Validation of FAVOR Code Linear Elastic Fracture Solutions for Finite-Length Flaw Geometries," *Fatigue and Fracture Mechanics in Pressure Vessels and Piping*, PVP-Vol. 304 (American Society of Mechanical Engineers, 1995).[‡]

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

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

‡ Available in public technical libraries.

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 Fracture-Mode Conversion Model Development (J. A. Keeney and B. R. Bass)

4.2.1 Computer Program Development (R. H. Dodds, Jr., UI)

The development of robust approaches to model ductile tearing remains an open issue and a prerequisite to understanding the complex behavior observed in resistance curves of common fracture specimens and structural components. Often driven by regulatory agencies, current research focuses on ferritic steels operating on the upper shelf, where toughness values after a few millimeters of stable, ductile growth frequently exceed several times the toughness values at initiation of growth. Material microstructure, flow properties, flaw size, geometry, and loading mode and rate all affect the macroscopic (Mode I) resistance curve characterized, for example, in terms of J - Δa . References 1 and 2 summarize experimental studies that demonstrate the complexity of R-curve behavior. To investigate each of these influences in a systematic manner, computational studies of fracture specimens and structural components must incorporate realistic models for the process of ductile tearing. Such models have evolved along essentially two lines of development: (1) the use of continuum damage theories of plasticity that reduce the stress

capacity of material ahead of the extending crack and (2) the use of the crack-tip opening angle (CTOA) as a scalar measure of current plastic deformation ahead of the tip leading to growth. Both approaches have been successful to various degrees in a 2-D framework; however, 3-D studies remain almost nonexistent because of the modeling complexity and computational cost. Recent improvements in finite-element solution strategies coupled with more readily available supercomputers now make possible some preliminary evaluations of these approaches in a fully 3-D framework. An understanding of numerical models for ductile crack growth is essential to support ongoing HSST programs that are investigating the ductile-to-cleavage conversion in pressure vessel steels. Ductile tearing often represents the precursor to cleavage fracture and can have strong effects on the near-tip stress fields that trigger macroscopic cleavage. Successful correlations of experimental data (e.g., fracture toughness values for clad beams with deep and shallow cracks) will require realistic modeling of ductile tearing.

These methods have been implemented into the finite-element computer program³ WARP3D, which is under development at UI. This research code analyzes very large 3-D solid models encountered in fracture mechanics studies of crack-tip fields and ductile crack growth. This work is described in "Finite Element Modeling of Ductile Crack Growth," (ORNL/NRC/LTR-96/12) by A. Gullerud and R. H. Dodds, Jr. (UI), which has been completed and is awaiting approval from the program monitor.

The implementation of the continuum damage mechanics approach presented in the letter report extends the "computational cell" concept of Xia and Shih^{4,5} into 3-D. Material softening and loss of stress capacity because of microvoid growth, followed by coalescence, which creates new crack surfaces, is described using a dilatant plasticity model from Gurson,⁶ as later modified by Tvergaard.⁷ When the smeared voids within a discrete "cell" grow to a critical fraction of the cell volume, the computational procedures eliminate the cell from the model, thus growing the crack at that point along the front by a discrete increment (e.g., the cell size). To model Mode I crack extension, cells correspond to a planar layer of finite elements on the crack plane—one finite element being assigned to each cell with the cell size roughly the spacing between the larger inclusions (e.g., MnS). The cell size thus provides the otherwise missing physical-length scale over which damage occurs,

Ductile-to-Cleavage

and it becomes, in effect, a material parameter considered in the calibration process.

The implementation of the critical CTOA approach extends to 3-D the well-known procedures for "node release" used extensively in 2-D models. When CTOA at a point along the crack front attains the user-specified value for additional growth, computational procedures release the displacement constraint and hold the crack closed at that point, thereby creating a new crack surface. In a 2-D setting, this process becomes remarkably simple to implement in a finite-element code; in 3-D, however, a realistic model must permit nonuniform growth along the front (tunneling). This introduces considerable complexity in defining and tracking the evolving crack front during growth and in defining the multiple opening angles possible at nodes along the current front. As is customary in CTOA models of growth, a simple von Mises (J_2 flow theory) plasticity model characterizes material response ahead of the crack front. Material adjacent to the crack front thus maintains full stress-carrying capacity consistent with the specified, uniaxial stress-strain curve.

Analyses using plane-strain and 3-D models of SENB specimens, and comparisons with measured resistance curves (J - Δa), illustrate the relative predictive capabilities

of the computational cell and CTOA approaches. For the plane-strain SENB analyses, parameters for the computational cell and CTOA approach are calibrated to bring the predicted R-curves into agreement with the experimental data for an $a/W = 0.6$ specimen. The crack-opening profiles obtained with the computational cell solution yielded initial estimates for CTOA at initiation of growth and CTOA for continued growth. Figure 4.1 indicates CTOA taken from the cell method solutions at increasing amounts of crack extension. Using growth governed by CTOA, an initiation angle of 90° and a continued growth angle of 17° produce the best agreement with the experimental R-curve. Figure 4.2 shows the predicted and experimental R-curves for the shallow-notch specimen ($a/W = 0.14$), where the predicted curves are derived from analyses using the parameters calibrated for the deep-notch specimen. The computational cell approach provides an R-curve in good agreement with the experimental data. The R-curve predicted by the CTOA approach clearly underestimates the actual tearing resistance (i.e., the calibrated CTOA value of 17° for the deep notch is too small for the shallow-notch configuration). These analyses confirm the geometry dependence of the CTOA values during ductile crack growth. For these materials and configurations, CTOA does not represent a suitable near-tip fracture parameter to study the "transferability" of resistance to material tearing. However, the computational cell approach uses micro-mechanical parameters related to metallurgical features of

Figure 4.1 J - Δa curves predicted by plane-strain SE(B) analyses and average of three measured curves, $a/W = 0.6$

Figure 4.2 J- Δa curves predicted by plane-strain SE(B) analyses and average of three measured curves, $a/W = 0.14$

the material; these should remain insensitive to constraint changes. This approach shows real promise as a reliable predictive tool to develop transferability models for geometry effects on R-curves.

In 3-D analyses of the SENB specimen, the computational cell approach has shown good agreement with experimentally measured crack profiles for both plane-sided and side-grooved configurations. The CTOA approach reveals strong meshing effects and significant constraint dependence of critical CTOA values for "standard" fracture specimens that have $\Delta a \ll B$. The difficulty arises with the multiple definitions for CTOA at points along the front to set the direction for local crack advance (i.e., ahead or in the thickness direction). This difficulty led to the focus on 3-D models of relatively thin, $W/B \approx 15$, SENB specimens where element aspect ratios on the crack plane may approach unity without incurring enormous computational costs. The last set of analyses described in the letter report illustrate the success of a CTOA approach to predict ductile crack extension for large amounts of growth ($\Delta a \approx 10B$) in thin aluminum panels, where "steady" conditions develop at the extending front.

4.2.2 Calibration of Simplified Gurson Model

In Task H.4, initial studies of ductile tearing models have focused on the "computational cell" concept for void growth and an element extinction capability for modeling crack growth. These special features for modeling pre-cleavage ductile tearing in pressure vessel steels are contained in the WARP3D finite-element computer code.³ Void nucleation and growth ahead of a stably tearing crack are incorporated into a finite-element model using computational cells with explicit length scales. Two principal input parameters for this method are the initial volume fraction f_0 (the initial void volume divided by the cell volume) and the characteristic length D associated with crack-plane elements. According to theory, these parameters are dependent only on the material and not on specimen geometry. To evaluate this model, ductile crack growth data (R-curves) have been generated from side-grooved IT-CT specimens taken from the ORNL Standardized Nuclear Unit Power Plant System (SNUPPS) weld and base material in Task H.4.1.⁸ The parameters, f_0 and D , are being calibrated for the material through an iterative process involving finite-element analyses of these CT specimens (Fig. 4.3). In these analyses, when f in the cell

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

incident on the current crack tip reaches a specified critical value f_E , the computational procedures remove the cell, thereby advancing the crack tip in discrete increments of the cell size. 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 beams⁹) 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 .

Three-dimensional, elastic-plastic calibration analyses are being performed on the 1T-CT specimens. One-quarter of the CT specimens (20% side grooved and $a/W = 0.63$) is represented in the finite-element model of Fig. 4.4(a). The

model has 9350 nodes and 7885 elements (8-node bricks), with square elements in the crack-tip region [Fig. 4.4(b)] and along the crack plane to permit uniform increments of crack extension. The finite-element model is loaded by displacement increments imposed on nodes at the location of the top of the load-pin hole (not modeled). The Gurson constitutive model is used for the elements along the crack plane where ductile tearing occurs, and the rest of the model uses the von Mises constitutive relation.

Calibration analyses for the weld material were carried out for a D of 254 μm (element size along crack plane is 127 μm) and f_E of 0.15. f_0 was varied to match the experimental load vs LLD and J vs Δa curves. A value of 0.002 for f_0 gave the best approximation to the measured J vs Δa curves for the weld material (a value of 0.0025 for f_0 resulted in too much crack extension after ≈ 1 mm of tearing). The calculated load vs displacement curve, shown

Figure 4.4 Three-dimensional finite-element model of 1T-CT specimen: (a) complete mesh and (b) crack-plane region

in Fig. 4.5, follows the same trend as the measured values. The calculated values for J and crack extension, as shown in Fig. 4.6, are close to the measured values in one of the CT tests. Values of these parameters from the CT specimen analyses will be transferred to the clad beam model to determine if they predict the response observed in test CB-1.5 (flaw located in weld material).

In the next reporting period, the Gurson parameters will be calibrated for the base material using the CT specimen model, and the 3-D finite-element models of clad beams will be generated.

Figure 4.5 Comparison of calculated and measured LLD for specimens fabricated from SNUPPS longitudinal weld material

Figure 4.6 Comparison of calculated and measured crack extension for specimens fabricated from SNUPPS longitudinal weld material

References

1. *Constraint Effects in Fracture*, ASTM STP 1171, E. M. Hackett, K. H. Schwalbe, and R. H. Dodds, Jr., Eds. (American Society for Testing and Materials, Philadelphia, 1993).*
2. *Constraint Effects in Fracture Theory and Applications: Second Volume*, ASTM STP 1244, M. Kirk and A. Bakker, Eds. (American Society for Testing and Materials, Philadelphia, 1995).*
3. K. C. Koppenhoefer et al., WARP3D: Dynamic Non-linear Analysis of Solids using a Preconditioned Conjugate Gradient Software Architecture, UILU-ENG-94-2017, Department of Civil Engineering, University of Illinois at Urbana-Champaign, November 1994.
4. L. Xia and C. F. Shih, "Ductile Crack Growth—I. A Numerical Study using Computational Cells with Microstructurally-Based Length Scales," *J. Mech. Phys. Solids* 43, 233–259 (1995).*
5. L. Xia and C. F. Shih, "Ductile Crack Growth—II. Void Nucleation and Geometry Effects on Macroscopic Fracture Behavior," *J. Mech. Phys. Solid* 43, 1953–1981 (1995).*
6. A. L. Gurson, "Continuum Theory of Ductile Rupture by Void Nucleation and Growth: Part I—Yield Criteria and Flow Rules for Porous Ductile Media," *J. Eng. Mater. Technol.* 99, 2–5 (1977).*
7. V. Tvergaard, "Material Failure by Void Growth to Coalescence," *Adv. Appl. Mech.* 27, 83–151 (1990).*
8. W. E. Pennell, Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Heavy-Section Steel Technology Program, Semiannual Progress Report for October 1995–March 1996," NUREG/CR-4219 (ORNL/TM-9593/V13&N1), 1997.†
9. J. A. Keeney, B. R. Bass, W. J. McAfee, and S. K. Iskander, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Preliminary Assessment of the Fracture Behavior of Weld Material in Full-Thickness Clad Beams," NUREG/CR-6228 (ORNL/TM-12735), October 1994.†

* Available in public technical libraries.

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

5 Fracture Analysis Methods Development and Applications

T. L. Dickson

5.1 Development of Stress-Intensity-Factor Influence Coefficients

A continuing objective of this subtask has been the development of a comprehensive data base of accurate validated stress-intensity-factor influence coefficients (SIFICs) for a range of inner-surface flaw geometries for prototypical domestic commercial pressurized-water reactor (PWR) and boiling-water reactor (BWR) vessel geometries. ABAQUS,¹ a nuclear quality assurance (NQA-1) certified multidimensional finite-element code with fracture mechanics capabilities has been used to generate the data base. The implementation of this SIFIC data base into predictive fracture mechanics computer codes, such as FAVOR,² facilitates the generation of accurate fracture mechanics solutions for the range of flaw geometries as may be required in structural integrity assessments of commercial U.S. PWRs and BWRs.

During previous reporting periods, a data base of SIFICs was generated for a range of axially and circumferentially oriented inner-surface flaw depths with aspect ratios of 2, 6, 10, and infinity, applicable to clad RPVs with inner-radius-to-wall thickness ratio (R_i/t) = 10 (Refs. 3-5). This geometry is prototypical of a large percentage of the commercial PWRs in the United States. The FAVOR code uses SIFIC methodology and superposition techniques to generate linear, elastic, fracture mechanics (LEFM) solutions for SIFs (K_I). For this prototypical PWR vessel geometry subjected to PTS transient loading conditions, FAVOR K_I solutions have been validated to be within 1-2% of ABAQUS solutions.⁶

More recently, ABAQUS has been used to generate a data base of SIFICs for a range of axially and circumferentially oriented inner-surface flaw depths with aspect ratios of 2, 6, and 10, applicable to clad RPVs with (R_i/t) = 20. This geometry is prototypical of a high percentage of the domestic commercial BWR vessels.⁷ During this reporting period, the implementation of this data base of SIFICs into the FAVOR code was validated. Again, FAVOR K_I solutions were validated by comparison with direct ABAQUS 3-D solutions for a set of benchmark problems. For this prototypical BWR vessel geometry subjected to transient loading conditions, FAVOR K_I solutions have been validated to be within 1-2% of ABAQUS solutions.

The objective of developing a comprehensive data base of accurate validated SIFICs for a range of inner-surface flaw geometries for prototypical domestic commercial PWR and BWR vessel geometries has now been completed.

5.2 PTS Applications

NRC is currently considering revising RG 1.154⁸ to provide guidance for two acceptable methods of performing a PTS analysis. The "method 1" PTS analysis would continue to specify that all flaws are inner-surface-breaking flaws. The "method 2" PTS analysis relaxes the method 1 conservatism by including the uncertainty associated with the location of the flaws inside the RPV wall (i.e., the inclusion of embedded as well as surface-breaking flaws for welded regions). The intent is that the licensee will have the option of performing a method 2 analysis if the method 1 analysis results do not satisfy the RG 1.154 acceptance criterion.

NRC is also considering the inclusion of the uncertainty associated with thermal-hydraulic boundary conditions into the method 2 PTS analysis. This report does not address this issue; rather, it describes a methodology currently under consideration to include in the probabilistic fracture mechanics (PFM) analysis the uncertainty associated with the location of the flaws within welded regions of the RPV wall. This methodology uses the RR-PRODIGAL computer code,^{9,10} an expert-based system developed by Rolls Royce, Inc., in the United Kingdom, that predicts flaw densities, flaw sizes, as well as the distribution of the flaw locations within the RPV wall for welded regions. During this reporting period, a software interface was developed such that output data files from RR-PRODIGAL can be used as input data into a specialized version of the FAVOR fracture analysis computer code, which can perform PFM analyses for surface and/or subsurface flaws.

The current RR-PRODIGAL simulation model addresses flaws within the welds of vessels but not flaws associated with the cladding on the vessel inner surface. Work is currently in progress to address such flaws. There is also a need to develop a simulation model to estimate flaws in the base metal of the plate and forged materials of RPVs.

Fracture

Predictions of the RR-PRODIGAL computer code have been compared with data from actual vessel welds to establish confidence in the model. Such data are available from laboratory and field NDE of vessels, along with destructive examinations of samples taken from regions of vessels that have been identified from NDE as having flaw indications. Pacific Northwest National Laboratory has examined welds and base material in the PVRUF vessel located at ORNL.¹¹ This vessel was fabricated from a PWR never put into service. The Synthetic Aperture Focusing Technique for Ultrasonic Testing (SAFT-UT) was used to detect and characterize the flaws created during fabrication of the vessel. The RR-PRODIGAL model underestimated the flaw density by a factor of about 10, when it considered all of the detected flaws in the full thickness. However, when the comparison was limited to flaws greater than 4 mm in depth, the measured flaw data agreed quite well with the predictions of the RR-PRODIGAL simulation.

During this reporting period, PTS analyses were performed to test the PRODIGAL-FAVOR software interface and the specialized version of the FAVOR fracture analysis computer code. This provided an opportunity to compare PTS analysis results generated by the two PTS analysis methodologies currently under consideration and, therefore, to determine the potential impact that the inclusion of embed-

ded flaws could have on the results of a PTS regulatory analysis.

RR-PRODIGAL categorizes the location of the flaw inner crack-tip relative to the RPV inner surface as follows:

Category 1 = inner surface-breaking flaws

Categories 2–5 = embedded flaws with inner crack-tip locations as follows:

Category 2— $t_{clad} < x < t/8$,

Category 3— $t/8 < x < 3t/8$,

Category 4— $3t/8 < x < t/2$,

Category 5— $t/2 < x < t$,

where

t_{clad} = thickness of clad,

t = thickness of vessel wall.

5.2.1 Example Problem Definition

RG 1.154 requires that the PFM analysis include the entire RPV beltline region, which is the region of the RPV extending just beyond the active length of the core, as illustrated in Fig. 5.1. FAVOR allows the RPV beltline to be divided into major regions such as plates, axial welds, and

Figure 5.1 Vessel beltline layout

circumferential welds. Each major RPV region may be further subdivided into subregions, each of which may be assigned its own distinguishing embrittlement and flaw parameters of neutron fluence at the inner surface of the RPV, copper and nickel concentrations, initial unirradiated RT_{NDT} (designated as RT_{NDT0}), flaw density, volume, flaw orientation and aspect ratio, and whether to use the RG 1.99 Rev. 2 (Ref. 12) weld or plate correlation for predicting the amount of radiation-induced shift in RT_{NDT} .

For illustration, the RPV beltline in Fig. 5.1 is divided into 8 major regions and further subdivided into 20 subregions. A hypothetical embrittlement map for the RPV beltline, shown in Fig. 5.1, is specified in Table 5.1. The value of RT_{NDTs} is the value of RT_{NDT} at the inner surface of the vessel calculated according to RG 1.99, Rev. 2 (Ref. 12). The value of $RT_{NDTs} + 2\sigma$ is the value of RT_{NDTs} plus an

uncertainty term of 2σ . In this example problem, the limiting beltline region is subregion 4. The value of $RT_{NDTs} + 2\sigma$ for subregion 4 is equal to the current PTS screening criteria ($RT_{NDTs} = 270^\circ\text{F}$ for axial welds and plates), requiring that an RG 1.154⁸ analysis be performed for continued operation.

5.2.2 Flaw Distributions

For a method 1 PTS analysis, the total number of flaws in the model is determined from the user-specified values of flaw density and volume assigned to each RPV subregion. The product of the flaw density and volume of each subregion determines the number of flaws in that subregion. The summation of those products over all modeled subregions is, of course, the total number of flaws in the entire model as follows:

Table 5.1 RPV beltline embrittlement map for illustrative PFM analysis

Major regions	Subregions	Copper (%)	Nickel (%)	Neutron fluence (10^{19} neutrons/cm ²)	RT_{NDT0} (°F)	$RT_{NDTs} + 2\sigma^a$ (°F)
1. Upper axial welds	1	0.22	0.54	1.14	-56	173
	2	0.22	0.54	1.34	-56	180
	3	0.22	0.54	1.04	-56	169
2. Intermediate axial welds	4	0.27	0.67	3.62	-56	270
	5	0.27	0.67	2.93	-56	260
	6	0.27	0.67	3.13	-56	263
3. Upper circular weld	7	0.22	0.67	1.70	-56	214
4. Lower circular weld	8	0.22	0.54	2.00	-56	197
5. Lower axial welds	9	0.22	0.67	2.00	-56	222
	10	0.22	0.67	2.20	-56	226
	11	0.22	0.67	1.20	-56	197
6. Lower shell	12	0.15	0.17	2.00	33	181
	13	0.15	0.17	2.12	50	199
	14	0.15	0.17	1.73	19	158
7. Intermediate shell	15	0.17	0.15	3.80	33	201
	16	0.13	0.15	2.80	30	171
	17	0.15	0.15	2.90	45	198
8. Upper shell	18	0.17	0.13	1.80	30	181
	19	0.18	0.13	1.70	29	183
	20	0.10	0.13	1.63	36	151

^aValue of RT_{NDT} at the inner surface of the vessel plus uncertainty of 2σ . For welded regions, $2\sigma = 65.5^\circ\text{F}$; for plate regions, $2\sigma = 59^\circ\text{F}$.

Fracture

$$\text{Total flaws} = \sum_{i=1}^{\text{NSUB}} (\text{subregion volume})_i \times (\text{subregion flaw density})_i, \quad (1)$$

where NSUB = the total number of subregions included in the model.

In a method 2 analysis, the number and distribution of flaws in the plate regions are determined in the same manner as is done for a method 1 analysis (i.e., the sum of the product of the user-specified NPLATE subregion plate volumes and the plate flaw densities, where NPLATE is the number of plate subregions included in the model).

$$\text{Plate flaws} = \sum_{i=1}^{\text{NPLATE}} (\text{plate volume})_i \times (\text{plate flaw density})_i, \quad (2)$$

In a method 2 analysis, the number and distribution of flaws in welded regions are determined from the RR-PRODIGAL code. Table 5.2 is an example RR-PRODIGAL output data file. An RR-PRODIGAL output file is a 100 by 5 array of flaw densities expressed in flaws per meter length of weld. The columns of the array correspond to Categories 1–5 (i.e., column 1 corresponds to flaw Category 1, column 2 corresponds to flaw Category 2, etc.). The row numbers (1–100) correspond to the fractional RPV wall thickness [i.e., flaws in row i are of depth $(i/100)t$, where t is the RPV wall thickness].

Therefore, each entry in the array [PRODIGAL(i,j)] is the flaw density, expressed in flaws per meter, corresponding to Category j flaws with a depth of $(i/100)t$. In this case, only the first 15 rows of the array are shown because all rows greater than 15 contain only zeros (i.e., no flaws have a depth greater than 15% of the RPV wall thickness).

FAVOR reads the RR-PRODIGAL file and performs the following calculations before performing a PFM analysis. The weld flaw density for each flaw category is determined from the RR-PRODIGAL data by summing over the flaw densities for all 100 flaw depths.

$$\text{Weld flaw for Category 1 flaws} = \sum_{j=1}^{100} \text{PRODIGAL}(j,i), \quad (3)$$

The total weld flaw density for all flaw categories is derived by summing over the five category flaw densities of the PRODIGAL data as follows:

$$\text{Total weld flaw density} = \sum_{i=1}^5 \sum_{j=1}^{100} \text{PRODIGAL}(j,i), \quad (4)$$

The total weld length in the model is the sum of the NWELD user-specified weld lengths, where NWELD is the number of welds in the model. Therefore, the number of weld flaws in each flaw category is the product of the weld flaw density for each flaw category and the total flaw length as follows:

Table 5.2 An example RR-PRODIGAL output file

Category				
1	2	3	4	5
0.741134×10^{-6}	0.239197×10^{-2}	0.245619×10^{-2}	0.481859×10^{-2}	0.874304×10^{-2}
0.186019×10^{-4}	0.336680×10^{-1}	0.401409×10^{-1}	0.147406×10^{-1}	0.123509×10^0
0.918478×10^{-4}	0.105891×10^0	0.129028×10^0	0.491753×10^{-1}	0.393371×10^0
0.235270×10^{-4}	0.324969×10^{-1}	0.412639×10^{-1}	0.175589×10^{-1}	0.120444×10^0
0.893488×10^{-6}	0.536716×10^{-2}	0.888994×10^{-2}	0.333360×10^{-2}	0.183421×10^{-1}
0.181730×10^{-5}	0.453156×10^{-2}	0.783155×10^{-2}	0.279496×10^{-2}	0.163865×10^{-1}
0.237952×10^{-6}	0.159893×10^{-2}	0.258207×10^{-2}	0.103703×10^{-2}	0.513699×10^{-2}
0.105009×10^{-6}	0.210191×10^{-3}	0.504545×10^{-3}	0.167964×10^{-3}	0.752994×10^{-3}
0.110494×10^{-6}	0.251444×10^{-3}	0.300719×10^{-3}	0.125321×10^{-3}	0.558899×10^{-3}
0.169832×10^{-7}	0.285362×10^{-4}	0.637678×10^{-4}	0.456927×10^{-4}	0.192019×10^{-3}
0.000000×10^0	0.943326×10^{-6}	0.142727×10^{-5}	0.479434×10^{-6}	0.126593×10^{-4}
0.000000×10^0	0.458956×10^{-6}	0.156424×10^{-4}	0.903168×10^{-6}	0.135408×10^{-5}
0.000000×10^0	0.436769×10^{-6}	0.000000×10^0	0.000000×10^0	0.106223×10^{-4}
0.000000×10^0	0.000000×10^0	0.422933×10^{-6}	0.000000×10^0	0.847793×10^{-6}
0.000000×10^0	0.000000×10^0	0.000000×10^0	0.000000×10^0	0.834138×10^{-6}

$$\text{Weld flaws in Category } i = \sum_{j=1}^{100} \text{PRODIGAL}(j,i) \\ \times \sum_{k=1}^{\text{NWELD}} (\text{weld flaw length}) \quad (5)$$

The total number of weld flaws included in the model is the summation of the number of weld flaws in each category summed over the five flaw categories as follows:

$$\text{Total weld flaws} = \sum_{i=1}^5 \left\{ \sum_{j=1}^{100} \text{PRODIGAL}(j,i) \right. \\ \left. \times \sum_{k=1}^{\text{NWELD}} (\text{weld flaw length}) \right\} \quad (6)$$

Applying the above equations to the example RR-PRODIGAL file in Table 5.2 results in the total weld flaw density and flaw distributions by category illustrated in Table 5.3. The total weld flaw density is

1.201 flaws/meter length of weld. Also, applying the above equations to the subregion volumes and weld lengths, as specified in Table 5.4, and assuming plate and weld flaw densities of 0.1 and 1.0 flaw/m³, respectively, results in the flaw distributions, as shown in Table 5.4. As specified in Table 5.4, the total number of flaws in the method 1 analysis is 1.542, all of which are surface-breaking flaws. The total number of flaws in the method 2 analysis is 41.947, of which 99.99% are embedded flaws.

Obviously, a fractional part of a flaw cannot be modeled; however, an equivalent problem can be solved. FAVOR contains logic for handling fractional flaws, that is, when the model contains a noninteger number of flaws. For example, in the method 1 analysis, the total number of flaws in this model is 1.542. FAVOR PFM analyses work in increments of 10,000 vessels. That is, it checks for convergence and/or the maximum number of vessels to be simulated after each 10,000 simulated vessels, each with an integer number of flaws: 10,000 vessels, each having

Table 5.3 Flaw distributions by categories for welded regions

Flaw category	Weld flaw density (flaws/m of weld)	Number of flaws	Percentage of weld flaws	Cumulative distribution
1	0.00014	0.005	0.012	0.00012
2	0.18644	6.323	15.52	0.15538
3	0.23308	7.905	19.41	0.34949
4	0.09380	3.182	7.811	0.42761
5	<u>0.68746</u>	<u>23.311</u>	57.25	1.00000
Totals	1.201	40.726		

Table 5.4 Flaw distributions used in illustrative problem for both methods of PTS analysis

Major regions	Subregions	Volume (ft ³)	Length		Number of flaws	
			Feet	Meters	Method 1	Method 2
Upper axial welds	1-3	0.42	5.41	1.65	0.012	1.982
Intermediate axial welds	4-6	3.180	18.85	5.74	0.089	6.893
Lower axial welds	9-11	0.840	4.99	1.52	0.024	1.825
Upper circumference weld	7	3.500	41.00	12.5	0.098	15.013
Lower circumference weld	8	3.500	41.00	12.5	<u>0.098</u>	<u>15.013</u>
Weld totals					0.321	40.726
Lower shell	12-14	71.00			0.199	0.199
Intermediate shell	15-17	330.00			0.924	0.924
Upper shell	18-20	35.00			<u>0.098</u>	<u>0.098</u>
Plate totals					1.221	1.221
Grand totals					1.542	41.947

Fracture

0.542 flaw, would contain the same number of flaws as 5420 vessels each having 1 flaw. Therefore, simulating 15,420 vessels, each with 1 flaw, results in simulating the same number of flaws (is equivalent) as 10,000 vessels each with 1.542 flaws.

As another example, in the method 2 analysis, the number of flaws in the model totals 41.947. A total of 10,000 vessels, each having 0.947 flaws, would contain the same number of flaws as 231 vessels, each having 41 flaws $[(10000)(0.947)/41 \sim 231]$. Therefore, simulating 10,231 vessels, each with 41 flaws, results in simulating very nearly the same number of flaws (is equivalent) as 10,000 vessels each with 41.921 flaws.

Illustrative PFM analyses were performed using the two analysis methodologies. For simplicity, the analysis assumes a single transient. The transient definition is the one used in the NRC/Electric Power Research Institute (EPRI) cosponsored PTS benchmarking exercise.¹³ The thermal transient is characterized by a stylized, exponentially decaying coolant temperature. The following formulation is used for the exponentially decaying thermal transient:

$$T(t) = T_f + (T_i - T_f) \exp(-\beta \times \text{time}) ,$$

where:

- $T(t)$ = coolant temperature at time t ,
- T_f = final coolant temperature ($^{\circ}\text{C}$),
- T_i = coolant temperature ($^{\circ}\text{C}$) at time = 0,
- β = exponential decay constant (min^{-1}).

The final coolant temperature was 150°F ; β , the exponential decay constant, was 0.15 min^{-1} . The initial temperature was 550°F . The pressure is constant and is equal to 1 ksi.

In the method 1 analysis, all flaws are surface-breaking. The flaw densities are 1 and 0.1 flaws/m^3 for weld and plate regions, respectively. All flaws have an aspect ratio of infinity (infinite-length flaws). Flaws occurring in plate regions and axial weld regions are axially oriented. Flaws occurring in circumferential welds are circumferentially oriented. The Marshall flaw size distribution function (without inspection) is used for simulating flaw depth. This analysis is very similar to the methodology currently proposed in RG 1.154.

The method 2 analysis includes embedded flaws in the welded regions. The category of each flaw that resides in a welded region is predicted from a cumulative distribution function, as shown Table 5.3. The depth of each flaw is determined from the cumulative distribution function associated with that flaw category. The aspect ratio is determined from a uniform distribution with possibilities of 2, 6, 10, and infinity. The location of the flaw within the RPV wall thickness associated with the flaw category is sampled from a uniform distribution. For example, the inner tip of a Category 2 flaw has equal probability of occurring at any location in $t/4 < x < t/8$. FAVOR checks embedded flaws for cleavage initiation at their inner crack tip. If the flaw initiates, it is assumed to propagate through the clad, thus becoming an infinite-length flaw. Currently, plate flaws are treated the same as in method 1. That is, they are surface-breaking flaws, a flaw density of 0.1 flaw/m^3 is assumed, and the flaw depth is determined from the Marshall distribution.

The values of K_I for embedded flaws are calculated according to the methodology specified in Ref. 14.

5.2.3 Analysis Results

The results of the PFM analyses are plotted in Fig. 5.2, which depicts a plot of the frequency of RPV failure as a function of the limiting RT_{NDT} s, that is, the RT_{NDT} s of the most limiting region of the RPV beltline. In this illustrative case, the limiting region is subregion 4 (see Table 5.1). The frequency of vessel failure is the product of the mean event frequency and the conditional probability of vessel failure as determined by PFM analyses. The transient was assumed to have a mean event frequency of 0.0001 transient/reactor year. In Fig 5.2, the horizontal line corresponding to a frequency of vessel failure of 5×10^{-6} represents the maximum allowable value specified by RG 1.154. Therefore, the intersection of the frequency-of-failure curve with the maximum allowable line determines the maximum allowable values of RT_{NDT} of the limiting region that complies with the acceptance criterion.

For the method 1 PTS analysis, the values of the limiting RT_{NDT} s corresponding to 5×10^{-6} are 255 and 265°F , respectively. In the first case, all flaws were assumed to be infinite length; in the second case, aspect ratios were sampled from a uniform distribution of 2, 6, 10, and infinity. Therefore, in this illustrative problem, the results of PFM method 1 do not comply with the acceptance criterion. The results of PFM method 2, which includes embedded flaws,

Figure 5.2 Results of example regulatory PTS analyses

do comply with the acceptance criterion. In this case, for the method 2 analysis, the RPV would be in compliance with the acceptance criterion until the RTNDTs of limiting subregion 4 are equal to 280°F.

A method to calculate the remaining amount of reactor operating time until the frequency of RPV failure exceeds the acceptance criterion is as follows: assuming that neutron fluence varies linearly with the operating time of the reactor [reactor operating time is designated as effective full-power years (EFPY)], the time in the life of the vessel when the frequency of vessel failure for the method 2 analysis will be equal to the maximum acceptance value of 5×10^{-6} can be calculated as follows:

$$\text{EFPY}_{\text{max}} = f_{\text{max}} \text{EFPY}_{\text{PTS}} / f_{\text{PTS}},$$

where

EFPY_{max} = the time in the reactor operating life when the frequency of RPV failure is equal to the acceptance criterion,
 f_{max} = the neutron fluence of the limiting region of the RPV beltline when the frequency of RPV failure reaches the acceptance criterion (in this example case, the

fluence of subregion 4 is 4.6 when RTNDTs = 280°F),

EFPY_{PTS} = the time in the operating life of the vessel when the RTNDTs of the limiting region of the RPV are equal to the PTS screening criteria,

f_{PTS} = the neutron fluence of the limiting region of the RPV beltline when that region reaches the PTS screening criteria (in this example case, the neutron fluence of the limiting subregion 4 was 3.62; see Table 5.1).

For this example case, EFPY_{max} is 1.27 EFPY_{PTS}; therefore, the method 2 PTS analysis, currently under consideration by the NRC, results in an additional period of reactor operating time equal to 27% of the time that it took to reach the PTS screening criteria (RTNDTs = 270°F).

In all method 2 PTS analyses performed thus far, the only embedded flaws predicted to initiate in cleavage fracture were Category 2 flaws (i.e., flaws that have their inner crack tip in the region between the clad-base interface and 1/8 of the RPV wall thickness). The mean value for the inner crack-tip position and the flaw depth of Category 2

Fracture

embedded flaws predicted to initiate in cleavage fracture were 0.37 and 0.31 in., respectively.

The same PFM analysis was performed using two other RR-PRODIGAL files in which the predicted totals of flaws in the weld regions were 66.51 and 571.27. For the case of 571.27 flaws, the method 2 PTS analysis resulted in no additional reactor operating life. For the case of 66.51 flaws, the method 2 PTS analysis results in an additional period of reactor operating time equal to 30% of the time that it took to reach the PTS screening criteria. In this case, even though the number of flaws was greater than the illustrative case analyzed previously (which contained 40.921 flaws), the flaw size and location distributions were such that the method 2 analysis resulted in a longer additional reactor operating time.

5.3 NRC Support

During this reporting period, work was performed reviewing a licensee response to previous HSST questions regarding PFM methodology and analysis results of the VIPER computer code, as detailed in Ref. 15.

Benchmark K_I calculations were performed for an exercise of the WGOPC. The HSST Program Manager is a member and an active participant of this working group.

References

1. *ABAQUS User Manual*, Hibbit, Karlsson & Sorenson, Inc., Providence, R. I. (1995).
2. T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "FAVOR: A Fracture Analysis Code for Nuclear Reactor Pressure Vessels, Release 9401," ORNL/NRC/LTR 94-1, February 1994.*
3. J. W. Bryson and T. L. Dickson, "Stress Intensity Factor Influence Coefficients for Axial and Circumferential Flaws in Reactor Pressure Vessels," *Pressure Vessel Integrity*, Vol. 250 (American Society of Mechanical Engineers, 1993), pp. 77-88.†
4. J. W. Bryson and T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Stress-Intensity-Factor Influence Coefficients for Circumferentially Oriented Semielliptical Inner-Surface Flaws in Clad Vessels ($R_i/t = 10$)," ORNL/NRC/LTR-94/8, April 1994.*
5. J. A. Keeney and T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge National Laboratory, "Stress-Intensity-Factor Influence Coefficients for Axially Oriented Semielliptical Inner-Surface Flaws in Clad Vessels ($R_i/t = 10$)," ORNL/NRC/LTR-93/33-Rev. 1, September 1995.*
6. T. L. Dickson, "Validation of FAVOR Code Linear Elastic Fracture Solutions for Finite-Length Flaw Geometries," *Fatigue and Fracture Mechanics in Pressure Vessels and Piping* (American Society of Mechanical Engineers, 1995).†
7. ASME Sect. XI, *Task Group on Reactor Vessel Integrity Requirements*, "White Paper on Reactor Vessel Integrity Requirements for Level A and B Conditions," EPRI Project 2975-13, TR-100251, Electric Power Research Institute, January 1993.‡
8. U.S. Nuclear Regulatory Commission, Regulatory Guide 1.154, "Format and Content of Plant-Specific Pressurized Thermal Shock Safety Analysis Reports for Pressurized-Water Reactors."***
9. O. J. V. Chapman, *Simulation of Defects in Weld Construction, Reliability and Risk in Pressure Vessels and Piping*, PVP-Volume 251 (American Society of Mechanical Engineers, 1993), pp. 81-89.†
10. O. J. V. Chapman et al., "A Simulation Model for Estimating Probabilities of Defects in Welds," *Fatigue and Fracture*, Vol. 1, PVP-Vol. 323 (American Society of Mechanical Engineers, 1996), pp. 375-391.†
11. S. R. Doctor, "Experience in Determining Fabrication Flaw Distributions in a Reactor Pressure Vessel," in *Proc. 4th International Conference on Nuclear Engineering, March 10-14, 1996, New Orleans, Louisiana*, American Society of Mechanical Engineers, Japan Society of Mechanical Engineers.
12. U.S. Nuclear Regulatory Commission, Regulatory Guide 1.99, Rev. 2, "Radiation Embrittlement of Reactor Vessel Materials."***

13. K. Balkey et al., *Documentation of Probabilistic Fracture Mechanics Codes Used for Reactor Pressure Vessels Subjected to Pressurized Thermal Shock Loading*, EPRI TR-105001, Palo Alto, Calif., June 1995.[‡]
14. R. C. Cipola et al., *Failure Analysis Associates, Computational Method to Perform the Flaw Evaluation Procedure as Specified in the ASME Code, Sect. XI, Appendix A*, EPRI NP-1181, Palo Alto, Calif., September 1979.[‡]
15. Electric Power Research Institute, "Benchmarking of Probabilistic Fracture Mechanics Calculations," Chap. 8 in *BWR Vessel and Internals Project, BWR Reactor Pressure Vessel Shell Weld Inspection Recommendations*, TR-105697, Palo Alto, Calif.[‡]

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

† Available in public technical libraries.

‡ Available from EPRI for a fee.

** Copies are available from the 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)

All testing and analyses for the A 302 Grade B (modified) J-R evaluation task have been completed. A summary of results was presented in Ref. 1 (April–September 1995). Volume 1 of the final report was completed and sent to NRC for publication in September 1996. The abstract from that report was reproduced in the previous semiannual report.² Volume 2, which contains all the detailed data, including load-displacement traces, has been completed and was submitted to the ORNL Publishing and Graphics Services for assembly into final format. It will be delivered to the NRC for publication in November.

6.2 Dynamic Fracture Toughness

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

All testing and analyses for the dynamic fracture-toughness testing project at the NSWC in Annapolis have been completed. The final report was delivered to ORNL for review in September and is undergoing review by various staff

members. Following the ORNL review, the report will be sent to NSWC for any necessary revisions and will then be prepared for final publication as a NUREG report.

References

1. R. K. Nanstad, Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Material Property Data and Test Methods," "Heavy-Section Steel Technology Program Semiannual Progress Report for April–September 1995," NUREG/CR-4219 (ORNL/TM-9593/V12&N2), Vol. 12, No. 2, 1996.*
2. R. K. Nanstad, Lockheed Martin Energy Research Corp., Oak Ridge National Laboratory, "Material Property Data and Test Methods," "Heavy-Section Steel Technology Program Semiannual Progress Report for October 1995–March 1996," NUREG/CR-4219 (ORNL/TM-9593/V13&N1), Vol. 13, No. 1, 1997.*

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

7 Integration of Results

J. G. Merkle

7.1 Relation Between Observed Locations of Cleavage Trigger Points and the Constraint Parameter M (D. E. McCabe)

7.1.1 Background

During its most recent ballot (July 1996), the ASTM draft standard, "Test Method for the Determination of Reference Temperature, T_0 , for Ferritic Steels in the Transition Range," received a negative ballot on an issue related to the maximum constraint loss that can be allowed before declaration of invalid data. The invalidity concept is based on a CTOD criterion expressed as follows:

$$K_{Jc(\text{limit})} = \left(E b_0 \sigma_{ys} / M \right)^{1/2}, \quad (1)$$

where

- b_0 = length of the remaining ligament in the specimen,
- σ_{ys} = material yield strength,
- M = a nondimensional coefficient.

The present M value of 30 was originally based on a simple estimation scheme that is supported by experimental evidence. The negative voter contended that the M value should be at least 50, based on the results of 3-D FEAs reported by Nevalainen and Dodds.¹ In that case, stochastic reasoning was applied to local crack-tip stress field analyses, defining an acceptable level of deviation from true singularity as a sufficient condition for constraint control. This issue was one of several issues discussed at an NRC staff and contractors meeting subsequently held in Rockville, Maryland, on October 22, 1996.

This chapter represents the follow-up work that resulted from a presentation by R. H. Dodds that included both the Nevalainen-Dodds crack-tip analyses and the results of a Monte Carlo study that evaluated the influence of M values on the accuracy of T_0 determinations. The latter study was performed in collaboration with C. Ruggieri. The Monte Carlo simulations illustrated how M values ranging from 30 to 50, applied over varied K_0 levels, can impact T_0 . In each case, sample size was used as the independent variable of the simulation. The results demonstrated that censoring data from precracked Charpy specimens and using $M = 50$ as the limitation in Eq. (1) yields the more conservative T_0 transition temperature. The curves in Fig. 7.1

Figure 7.1 Nevalainen-Dodds results converted to notation of draft standard

Integration

were used to illustrate the impact of the value of M selected on the constraint loss [departure from small-scale yielding (SSY)]. According to Fig. 7.1, the critical ratio of maximum principal stress, σ_1 , to material flow strength, σ_0 , plotted on the abscissa, should exercise control over the location of cleavage crack initiation (trigger) points. A rough correlation is suggested in Fig. 7.1, below the horizontal axis, between σ_1/σ_0 and the trigger point distance, r_c , normalized by CTOD. Hence, the model suggests that if cleavage triggering occurs predominantly at 6δ from the crack tip where the normalized stress ratio is about 3.2, and M lies between 23 and 71, then an M value of 30 used for $K_{Jc}(\text{limit})$ should be of concern. Conversely, the M value recedes into irrelevance if trigger points can be tracked to within $2\delta(\Delta)$ distance from the crack tip, where the normalized principal stress ratio at trigger points tends to a value of 3.6.

7.1.2 Application of Fractographic Data

A paper by Heerens et al.² has given ample experimental evidence about trigger points and their relationship to observed fracture toughness. Work leading to Ref. 2 was cited in Ref. 1 relative to the discussion of Fig. 7.1. Therefore, Ref. 2 was given a more thorough evaluation, and that evaluation will be presented here.

A pressure vessel steel (DIN20MnMoNi55) was selected, and 26 compact specimens (1T CT) were tested to develop J_c fracture-toughness values throughout the transition range. All specimens failed in cleavage. Fracture surfaces were examined for trigger points, and the distance from the forward boundary of the stretch zone ahead of the crack tip to the trigger point was measured in each case. Reference 2 also contains calculations of the maximum principal stress at the trigger point location, using an analytical expression for σ_1 developed by Schwalbe.³ Schwalbe's equation had been shown to closely approximate the results of finite-element solutions, at least through the distance range of primary interest. A slip line solution was added to provide an approximate estimate of the stresses in close proximity to and within the stretch zone. Hence, Refs. 2 and 3 provided the two critical criteria needed in the discussion of Fig. 7.1.

Figure 7.2 shows the experimentally determined trigger point distances given in Ref. 2, normalized to CTOD, Δ , for 23 of the 26 specimens tested. However, crack advance from the stretch zone, Δ_{ap} , was calculated and added to the Heerens r_c measurements. The blunting line equation of Paragraph 9.2.3 in ASTM E813-89 was used to determine Δ_{ap} . This expression was selected because of its known good accuracy by extensive experimental verification. On

Figure 7.2 Cumulative probability for 23 determinations of CTOD normalized trigger point distances

the other hand, CTOD (or δ) was calculated using $\delta = J/m\sigma_{ys}$ with the proportionality factor "m" set at 1.7 for crack-tip plane strain constraint. This factor had been derived by FEAs; consequently, the solutions are more likely to agree with the Nevalainen-Dodds δ values used in Fig. 7.1.

Three of the data were eliminated from the present evaluation because they stood out as obvious outliers from other well-duplicated test conditions. The test temperatures were -90 , -70 , -60 , and -55°C . The data shown in Fig. 7.2 include all four test temperatures. The r_c/Δ median is 2.9. The cumulative probability trend shown in Fig. 7.2 was then fitted with a three-parameter Weibull curve that was used to calculate a probability density function for comparison to the Nevalainen-Dodds plot shown again in Fig. 7.3. The shaded area represents 15% of the distribution. Because the curve for $M = 36$ intersects the line for $J_{avg}/J_0 = 1.2$ at the end of the shaded area, it is apparent that about 85% of all the trigger points correspond to $M < 36$. Therefore, neglecting the small difference between $M = 36$ and $M = 30$, about 85% of the experimental data appear to escape any anticipated constraint loss effects due to invalid specimen size from $M = 30$. Figure 7.4 indicates the specimens that are expected to have excessive constraint loss using $M = 30$ in Eq. (1), and Fig. 7.5 applies the same procedure using $M = 50$. It is difficult to select a relationship between r_c/Δ and Eq. (1) in both cases. The

Heerens et al. paper² listed $\sigma_1(S_1)$ values obtained at the trigger point location for each specimen tested. These values were normalized by material yield strength, as adjusted for test temperature, and the results are shown in Fig. 7.6. Obviously, there was no relationship between σ_1/σ_0 and r_c normalized by CTOD as had been implied in Fig. 7.1. Normalized cleavage stress is almost constant and never as low as 3.2. Therefore, experimental confirmation of the trend shown in Fig. 7.1 is not provided by the Heerens et al. paper.²

Figure 7.7 is a Weibull plot of the K_{Jc} data converted from J_c at one test temperature (-60°C), at which 14 specimens had been tested. This plot includes one outlier value by r_c not previously used. Despite the outlier, all data in Fig. 7.7 indicate that the data scatter characteristic of the test material used was quite normal for a pressure vessel steel. This result confirms that a weakest link effect was operable even well into the range of invalidity for $M = 50$. There was no deviation away from the characteristic full constraint portion of data at the highest fracture-toughness levels. Hence, a correction of measured K_{Jc} values to SSY equivalence for the highest toughness numbers that were in violation of the $M = 50$ criterion would serve only to distort the shape of an already well-behaved toughness distribution. These results, therefore, fail to make the case for use of $M = 50$ in the ductile-to-brittle test standard.

Figure 7.3 Figure 7.1 with inverted density function obtained from Weibull plot of Fig. 7.2. The shaded area on abscissa is the 15% probability level by r_c/δ .

Figure 7.4 Figure 7.2 with filled data points indicating failure to meet M = 30 constraint limit

Figure 7.5 Figure 7.2 with filled data points indicating failure to meet M = 50 constraint limit

Figure 7.6 Principal stress at cleavage trigger point normalized by yield strength vs CTOD normalized trigger point distance

Figure 7.7 Weibull plot of all K_{Jc} data determined at -60°C [slope = 4, $K_{Jc(\text{med})} = 240 \text{ MPa} \sqrt{\text{m}}$]

References

1. M. Nevalainen and R. H. Dodds, "Numerical Investigation of 3D Constraint Effects on Brittle Fracture in SE(B) and C(T) Specimens," *Int. J. Fract.* 74, 131-61 (1995).*
2. J. Heerens, D. T. Read, A. Cornec, and K. H. Schwalbe, "Interpretation of Fracture Toughness in the Ductile to Brittle Transition Region by Fractographic Observations," in *Defect Assessment in Components—Fundamentals and Applications*,ESIS/EGF9 (Mechanical Engineering Publications, London, 1991), pp. 659-78.
3. K. H. Schwalbe, written discussion of previously published paper by D. M. Tracey, "Finite Element Solutions for Crack-Tip Behavior in Small-Scale Yielding," *Trans. ASME, Series H, J. Eng. Mater. Technol.*, 99, 186-88 (April 1977).*

* Available in public technical libraries.

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	lb _f	224.809
kPa	psi	0.145038
MPa	ksi	0.145038
MPa·√m	ksi·√in.	0.910048
J	ft·lb	0.737562
K	°F or °R	1.8
kJ/m ²	in.-lb/in. ²	5.71015
W·m ⁻² ·K ⁻¹	Btu/h·ft ² ·°F	0.176110
kg	lb	2.20462
kg/m ³	lb/in. ³	3.61273 × 10 ⁻⁵
mm/N	in./lb _f	0.175127
T(°F) = 1.8(°C) + 32		

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

Prior Heavy-Section Steel Technology Program Publications

The work reported here was performed at Oak Ridge National Laboratory under the Heavy-Section Steel Technology (HSST) Program, W. E. Pennell, Program Manager. The program is sponsored by the Office of Nuclear Regulatory Research of the U.S. Nuclear Regulatory Commission (NRC). The technical monitor for NRC is S. N. M. Malik. Prior and future reports in this series are listed below.

1. S. Yukawa, General Electric Company, Schenectady, N.Y., *Evaluation of Periodic Proof Testing and Warm Prestressing Procedures for Nuclear Reactor Vessels*, HSSTP-TR-1, July 1, 1969.
2. L. W. Loechel, Martin Marietta Corporation, Denver, Colo., *The Effect of Testing Variables on the Transition Temperature in Steel*, MCR-69-189, November 20, 1969.
3. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Gross Strain Measure of Fracture Toughness of Steels*, HSSTP-TR-3, November 1, 1969.
4. C. Visser, S. E. Gabrielse, and W. VanBuren, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *A Two-Dimensional Elastic-Plastic Analysis of Fracture Test Specimens*, WCAP-7368, October 1969.
5. T. R. Mager and F. O. Thomas, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Evaluation by Linear Elastic Fracture Mechanics of Radiation Damage to Pressure Vessel Steels*, WCAP-7328 (Rev.), October 1969.
6. W. O. Shabbits, W. H. Pryle, and E. T. Wessel, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Heavy-Section Fracture Toughness Properties of A533 Grade B Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-7414, December 1969.
7. F. J. Loss, Naval Research Laboratory, Washington, D.C., *Dynamic Tear Test Investigations of the Fracture Toughness of Thick-Section Steel*, NRL-7056, May 14, 1970.
8. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Inc., Glenwood, Ill., *Crack Arrest Fracture Toughness of A533 Grade B Class 1 Pressure Vessel Steel*, HSSTP-TR-8, March 1970.
9. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Post-Irradiation Testing of 2T Compact Tension Specimens*, WCAP-7561, August 1970.
10. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Fracture Toughness Characterization Study of A533, Grade B, Class 1 Steel*, WCAP-7578, October 1970.
11. T. R. Mager, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Notch Preparation in Compact Tension Specimens*, WCAP-7579, November 1970.
12. N. Levy and P. V. Marcal, Brown University, Providence, R.I., *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase I: Simple Flawed Specimens*, HSSTP-TR-12, December 1970.
13. W. O. Shabbits, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *Dynamic Fracture Toughness Properties of Heavy Section A533 Grade B Class 1 Steel Plate*, WCAP-7623, December 1970.
14. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Gross Strain Crack Tolerance of A 533-B Steel*, HSSTP-TR-14, May 1, 1971.
15. H. T. Corten and R. H. Sailors, University of Illinois, Urbana, Ill., *Relationship Between Material Fracture Toughness Using Fracture Mechanics and Transition Temperature Tests*, T&AM Report 346, August 1, 1971.
16. T. R. Mager and V. J. McLaughlin, Westinghouse Electric Corporation, PWR Systems Division, Pittsburgh, Pa., *The Effect of an Environment of High Temperature Primary Grade Nuclear Reactor Water on the Fatigue Crack Growth Characteristics of A533 Grade B Class 1 Plate and Weldment Material*, WCAP-7776, October 1971.
17. N. Levy and P. V. Marcal, Brown University, Providence, R.I., *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics, Phase II: Improved Modelling*, HSSTP-TR-17, November 1971.
18. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-in.-Thick Flawed Tensile Specimens, First Technical Summary Report, Longitudinal Specimens Numbers 1 through 7*, HSSTP-TR-18, June 1972.

Prior

19. P. N. Randall, TRW Systems Group, Redondo Beach, Calif., *Effects of Strain Gradients on the Gross Strain Crack Tolerance of A533-B Steel*, HSSTP-TR-19, June 15, 1972.
20. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Flawed Tensile Specimens, Second Technical Summary Report, Transverse Specimens Numbers 8 through 10, Welded Specimens Numbers 11 through 13*, HSSTP-TR-20, June 1972.
21. L. A. James and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., Heavy Section Steel Technology Program Technical Report No. 21, *The Effect of Temperature and Neutron Irradiation Upon the Fatigue-Crack Propagation Behavior of ASTM A533 Grade B, Class 1 Steel*, HEDL-TME 72-132, September 1972.
22. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Flawed Tensile Specimens, Third Technical Summary Report, Longitudinal Specimens Numbers 14 through 16, Unflawed Specimen Number 17*, HSSTP-TR-22, October 1972.
23. S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Tensile Specimens, Fourth Technical Summary Report, Tests of 1-Inch-Thick Flawed Tensile Specimens for Size Effect Evaluation*, HSSTP-TR-23, June 1973.
24. S. P. Ying and S. C. Grigory, Southwest Research Institute, San Antonio, Tex., *Tests of 6-Inch-Thick Tensile Specimens, Fifth Technical Summary Report, Acoustic Emission Monitoring of One-Inch and Six-Inch-Thick Tensile Specimens*, HSSTP-TR-24, November 1972.
25. R. W. Derby, J. G. Merkle, G. C. Robinson, G. D. Whitman, and F. J. Witt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-Inch-Thick Pressure Vessels. Series 1: Intermediate Test Vessels V-1 and V-2*, ORNL-4895, February 1974.
26. W. J. Stelzman and R. G. Berggren, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Radiation Strengthening and Embrittlement in Heavy Section Steel Plates and Welds*, ORNL-4871, June 1973.
27. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Inc., Glenwood, Ill., *Crack Arrest in an Increasing K-Field*, HSSTP-TR-27, January 1973.
28. P. V. Marcal, P. M. Stuart, and R. S. Bettles, Brown University, Providence, R.I., *Elastic Plastic Behavior of a Longitudinal Semi-Elliptic Crack in a Thick Pressure Vessel*, HSSTP-TR-28, June 1973.
29. W. J. Stelzman, R. G. Berggren, and T. N. Jones, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORNL Characterization of Heavy-Section Steel Technology Program Plates 01, 02 and 03*, USNRC Report NUREG/CR-4092 (ORNL/TM-9491), April 1985.
30. Canceled.
31. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Irradiation and Temperature Dependence of Tensile and Fracture Properties of ASTM A533, Grade B, Class 1 Steel Plate and Weldment*, HEDL-TME 73-75, August 1973.
32. J. M. Steichen and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *High Strain Rate Tensile Properties of Irradiated ASTM A533 Grade B Class 1 Pressure Vessel Steel*, July 1973.
33. P. C. Riccardella and J. L. Swedlow, Westinghouse Electric Corporation, Pittsburgh, Pa., *A Combined Analytical-Experimental Fracture Study of the Two Leading Theories of Elastic-Plastic Fracture (J-Integral and Equivalent Energy)*, WCAP-8224, October 1973.
34. R. J. Podlasek and R. J. Eiber, Battelle Columbus Laboratories, Columbus, Ohio, *Final Report on Investigation of Mode III Crack Extension in Reactor Piping*, December 14, 1973.
35. T. R. Mager, J. D. Landes, D. M. Moon, and V. J. McLaughlin, Westinghouse Electric Corporation, Pittsburgh, Pa., *Interim Report on the Effect of Low Frequencies on the Fatigue Crack Growth Characteristics of A533 Grade B Class 1 Plate in an Environment of High-Temperature Primary Grade Nuclear Reactor Water*, WCAP-8256, December 1973.
36. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Irradiated Fracture Toughness of ASTM A533, Grade B, Class 1 Steel Measured with a Four-Inch-Thick Compact Tension Specimen*, HEDL-TME 75-10, January 1975.
37. R. H. Bryan, J. G. Merkle, M. N. Raftenberg, G. C. Robinson, and J. E. Smith, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-Inch-Thick Pressure Vessels. Series 2: Intermediate Test Vessels V-3, V-4, and V-6*, ORNL-5059, November 1975.

38. T. R. Mager, S. E. Yanichko, and L. R. Singer, Westinghouse Electric Corporation, Pittsburgh, Pa., *Fracture Toughness Characterization of HSST Intermediate Pressure Vessel Material*, WCAP-8456, December 1974.
39. J. G. Merkle, G. D. Whitman, and R. H. Bryan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Evaluation of the HSST Program Intermediate Pressure Vessel Tests in Terms of Light-Water-Reactor Pressure Vessel Safety*, ORNL/TM-5090, November 1975.
40. J. C. Merkle, G. C. Robinson, P. P. Holz, J. E. Smith, and R. H. Bryan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7*, USNRC Report ORNL/NUREG-1, August 1976.
41. J. A. Davidson, L. J. Ceschini, R. P. Shogan, and G. V. Rao, Westinghouse Electric Corporation, Pittsburgh, Pa., *The Irradiated Dynamic Fracture Toughness of ASTM A533, Grade B, Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-8775, October 1976.
42. R. D. Cheverton, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to a PWR LOCA-ECC Thermal Shock: Experiments TSE-1 and TSE-2*, USNRC Report ORNL/NUREG/TM-31, September 1976.
43. J. G. Merkle, G. C. Robinson, P. P. Holz, and J. E. Smith, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-In.-Thick Pressure Vessels. Series 4: Intermediate Test Vessels V-5 and V-9 with Inside Nozzle Corner Cracks*, USNRC Report ORNL/NUREG-7, August 1977.
44. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The Ductile Fracture Toughness of Heavy Section Steel Plate*, USNRC Report NUREG/CR-0859, September 1979.
45. R. H. Bryan, T. M. Cate, P. P. Holz, T. A. King, J. G. Merkle, G. C. Robinson, G. C. Smith, J. E. Smith, and G. D. Whitman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7A Under Sustained Loading*, USNRC Report ORNL/NUREG-9, February 1978.
46. R. D. Cheverton and S. E. Bolt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to a PWR LOCA-ECC Thermal Shock: Experiments TSE-3 and TSE-4 and Update of TSE-1 and TSE-2 Analysis*, USNRC Report ORNL/NUREG-22, December 1977.
47. D. A. Canonico, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Significance of Reheat Cracks to the Integrity of Pressure Vessels for Light-Water Reactors*, USNRC Report ORNL/NUREG-15, July 1977.
48. G. C. Smith and P. P. Holz, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Repair Weld Induced Residual Stresses in Thick-Walled Steel Pressure Vessels*, USNRC Report NUREG/CR-0093 (ORNL/NUREG/TM-153), June 1978.
49. P. P. Holz and S. W. Wismer, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Half-Bead (Temper) Repair Welding for HSST Vessels*, USNRC Report NUREG/CR-0113 (ORNL/NUREG/TM-177), June 1978.
50. G. C. Smith, P. P. Holz, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack Extension and Arrest Tests of Axially Flawed Steel Model Pressure Vessels*, USNRC Report NUREG/CR-0126 (ORNL/NUREG/TM-196), October 1978.
51. R. H. Bryan, P. P. Holz, J. G. Merkle, G. C. Smith, J. E. Smith, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-7B*, USNRC Report NUREG/CR-0309 (ORNL/NUREG-38), October 1978.
52. R. D. Cheverton, S. K. Iskander, and S. E. Bolt, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Applicability of LEFM to the Analysis of PWR Vessels Under LOCA-ECC Thermal Shock Conditions*, USNRC Report NUREG/CR-0107 (ORNL/NUREG-40), October 1978.
53. R. H. Bryan, D. A. Canonico, P. P. Holz, S. K. Iskander, J. G. Merkle, J. E. Smith, and W. J. Stelzman, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels, Series 3: Intermediate Test Vessel V-8*, USNRC Report NUREG/CR-0675 (ORNL/NUREG-58), December 1979.
54. R. D. Cheverton and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Application of Static and Dynamic Crack Arrest Theory to TSE-4*, USNRC Report NUREG/CR-0767 (ORNL/NUREG-57), June 1979.
55. J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *Tensile Properties of Irradiated and Unirradiated Welds of A533 Steel Plate and A508 Forgings*, USNRC Report NUREG/CR-1158 (ORNL/Sub/79-50917/2), July 1979.
56. K. W. Carlson and J. A. Williams, Hanford Engineering Development Laboratory, Richland, Wash., *The*

- Effect of Crack Length and Side Grooves on the Ductile Fracture Toughness Properties of ASTM A533 Steel*, USNRC Report NUREG/CR-1171 (ORNL/Sub/79-50917/3), October 1979.
57. P. P. Holz, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Flaw Preparations for HSST Program Vessel Fracture Mechanics Testing; Mechanical-Cyclic Pumping and Electron-Beam Weld-Hydrogen Charge Cracking Schemes*, USNRC Report NUREG/CR-1274 (ORNL/NUREG/TM-369), May 1980.
58. S. K. Iskander, Computer Sciences Div., Union Carbide Corp. Nuclear Div., Oak Ridge, Tenn., *Two Finite Element Techniques for Computing Mode I Stress Intensity Factors in Two- or Three-Dimensional Problems*, USNRC Report NUREG/CR-1499 (ORNL/NUREG/CSD/TM-14), February 1981.
59. P. B. Crosley and E. J. Ripling, Materials Research Laboratory, Glenwood, Ill., *Development of a Standard Test for Measuring K_{Ia} with a Modified Compact Specimen*, USNRC Report NUREG/CR-2294 (ORNL/Sub/81-7755/1), August 1981.
60. S. N. Atluri, B. R. Bass, J. W. Bryson, and K. Kathiresan, Computer Sciences Div., Oak Ridge Gaseous Diffusion Plant, Oak Ridge, Tenn., *NOZ-FLAW: A Finite Element Program for Direct Evaluation of Stress Intensity Factors for Pressure Vessel Nozzle-Corner Flaws*, USNRC Report NUREG/CR-1843 (ORNL/NUREG/CSD/TM-18), March 1981.
61. A. Shukla, W. L. Fournery, and G. R. Irwin, University of Maryland, College Park, Md., *Study of Energy Loss and Its Mechanisms in Homalite 100 During Crack Propagation and Arrest*, USNRC Report NUREG/CR-2150 (ORNL/Sub/79-7778/1), August 1981.
62. S. K. Iskander, R. D. Cheverton, and D. G. Ball, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-I, A Code for Calculating the Behavior of Flaws on the Inner Surface of a Pressure Vessel Subjected to Temperature and Pressure Transients*, USNRC Report NUREG/CR-2113 (ORNL/NUREG-84), August 1981.
63. R. J. Sanford, R. Chona, W. L. Fournery, and G. R. Irwin, University of Maryland, College Park, Md., *A Photoelastic Study of the Influence of Non-Singular Stresses in Fracture Test Specimens*, USNRC Report NUREG/CR-2179 (ORNL/Sub/79-7778/2), August 1981.
64. B. R. Bass, S. N. Atluri, J. W. Bryson, and K. Kathiresan, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OR-FLAW: A Finite Element Program for Direct Evaluation of K-Factors for User-Defined Flaws in Plate, Cylinders, and Pressure-Vessel Nozzle Corners*, USNRC Report NUREG/CR-2494 (ORNL/CSD/TM-165), April 1982.
65. B. R. Bass and J. W. Bryson, Oak Ridge Natl. Lab., Oak Ridge Tenn., *ORMGEN-3D: A Finite Element Mesh Generator for 3-Dimensional Crack Geometries*, USNRC Report NUREG/CR-2997, Vol. 1 (ORNL/TM-8527/V1), December 1982.
66. B. R. Bass and J. W. Bryson, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORVIRT: A Finite Element Program for Energy Release Rate Calculations for 2-Dimensional and 3-Dimensional Crack Models*, USNRC Report NUREG/CR-2997, Vol. 2 (ORNL/TM-8527/V2), February 1983.
67. R. D. Cheverton, S. K. Iskander, and D. G. Ball, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *PWR Pressure Vessel Integrity During Overcooling Accidents: A Parametric Analysis*, USNRC Report NUREG/CR-2895 (ORNL/TM-7931), February 1983.
68. D. G. Ball, R. D. Cheverton, J. B. Drake, and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-II, A Code for Calculating Behavior of 2-D and 3-D Surface Flaws in a Pressure Vessel Subjected to Temperature and Pressure Transients*, USNRC Report NUREG/CR-3491 (ORNL-5934), February 1984.
69. A. Sauter, R. D. Cheverton, and S. K. Iskander, Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Modification of OCA-I for Application to a Reactor Pressure Vessel with Cladding on the Inner Surface*, USNRC Report NUREG/CR-3155 (ORNL/TM-8649), May 1983.
70. R. D. Cheverton and D. G. Ball, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *OCA-P, A Deterministic and Probabilistic Fracture-Mechanics Code for Application to Pressure Vessels*, USNRC Report NUREG/CR-3618 (ORNL-5991), May 1984.
71. J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Examination of the Size Effects and Data Scatter Observed in Small Specimen Cleavage Fracture Toughness Testing*, USNRC Report NUREG/CR-3672 (ORNL/TM-9088), April 1984.
72. C. E. Pugh et al., Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Heavy-*

- Section Steel Technology Program—Five-Year Plan FY 1983–1987*, USNRC Report NUREG/CR-3595 (ORNL/TM-9008), April 1984.
73. D. G. Ball, B. R. Bass, J. W. Bryson, R. D. Cheverton, and J. B. Drake, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Stress Intensity Factor Influence Coefficients for Surface Flaws in Pressure Vessels*, USNRC Report NUREG/CR-3723 (ORNL/CSD/TM-216), February 1985.
 74. W. R. Corwin, R. G. Berggren, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Charpy Toughness and Tensile Properties of Neutron Irradiated Stainless Steel Submerged-Arc Weld Cladding Overlay*, USNRC Report NUREG/CR-3927 (ORNL/TM-9309), September 1984.
 75. C. W. Schwartz, R. Chona, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *SAMCR: A Two-Dimensional Dynamic Finite Element Code for the Stress Analysis of Moving Cracks*, USNRC Report NUREG/CR-3891 (ORNL/Sub/79-7778/3), November 1984.
 76. W. R. Corwin, G. C. Robinson, R. K. Nanstad, J. G. Merkle, R. G. Berggren, G. M. Goodwin, R. L. Swain, and T. D. Owings, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Effects of Stainless Steel Weld Overlay Cladding on the Structural Integrity of Flawed Steel Plates in Bending, Series I*, USNRC Report NUREG/CR-4015 (ORNL/TM-9390), April 1985.
 77. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, D. P. Edmonds, R. W. McCulloch, J. G. Merkle, R. K. Nanstad, G. C. Robinson, K. R. Thoms, and G. D. Whitman, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressurized-Thermal-Shock Test of 6-in.-Thick Pressure Vessels. PTSE-1: Investigation of Warm Pre-stressing and Upper-Shelf Arrest*, USNRC Report NUREG/CR-4106 (ORNL-6135), April 1985.
 78. R. D. Cheverton, D. G. Ball, S. E. Bolt, S. K. Iskander, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to the PWR Thermal-Shock Issue: Experiments TSE-5, TSE-5A, and TSE-6*, USNRC Report NUREG/CR-4249 (ORNL-6163), June 1985.
 79. R. D. Cheverton, D. G. Ball, S. E. Bolt, S. K. Iskander, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressure Vessel Fracture Studies Pertaining to the PWR Thermal-Shock Issue: Experiment TSE-7*, USNRC Report NUREG/CR-4304 (ORNL-6177), August 1985.
 80. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, J. G. Merkle, R. K. Nanstad, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Test of 6-in.-Thick Pressure Vessels. Series 3: Intermediate Test Vessel V-8A—Tearing Behavior of Low Upper-Shelf Material*, USNRC Report NUREG/CR-4760 (ORNL-6187), May 1987.
 81. R. D. Cheverton and D. G. Ball, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *A Parametric Study of PWR Pressure Vessel Integrity During Overcooling Accidents, Considering Both 2-D and 3-D Flaws*, USNRC Report NUREG/CR-4325 (ORNL/TM-9682), August 1985.
 82. E. C. Rodabaugh, E. C. Rodabaugh Associates, Inc., Hilliard, Ohio, *Comments on the Leak-Before-Break Concept for Nuclear Power Plant Piping Systems*, USNRC Report NUREG/CR-4305 (ORNL/Sub/82-22252/3), August 1985.
 83. J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORVIRT.PC: A 2-D Finite Element Fracture Analysis Program for a Microcomputer*, USNRC Report NUREG/CR-4367 (ORNL-6208), October 1985.
 84. D. G. Ball and R. D. Cheverton, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Adaptation of OCA-P, A Probabilistic Fracture-Mechanics Code, to a Personal Computer*, USNRC Report NUREG/CR-4468 (ORNL/CSD/TM-233), January 1986.
 85. J. W. Bryson and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORMGEN.PC: A Microcomputer Program for Automatic Mesh Generation of 2-D Crack Geometries*, USNRC Report NUREG/CR-4475 (ORNL-6250), March 1986.
 86. G. D. Whitman, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Historical Summary of the Heavy-Section Steel Technology Program and Some Related Activities in Light-Water Reactor Pressure Vessel Safety Research*, USNRC Report NUREG/CR-4489 (ORNL-6259), March 1986.
 87. C. Inversini and J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORPLOT PC: A Graphic Utility for*

- ORMGEN.PC and ORVIRT.PC*, USNRC Report NUREG/CR-4633 (ORNL-6291), June 1986.
88. J. J. McGowan, R. K. Nanstad, and K. R. Thoms, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Characterization of Irradiated Current-Practice Welds and A533 Grade B Class 1 Plate for Nuclear Pressure Vessel Service*, USNRC Report NUREG/CR-4880 (ORNL/TM-10387), July 1988.
89. K. V. Cook and R. W. McClung, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Flaw Density Examinations of a Clad Boiling Water Reactor Pressure Vessel Segment*, USNRC Report NUREG/CR-4860 (ORNL/TM-10364), April 1987.
90. D. J. Naus, B. R. Bass, C. E. Pugh, R. K. Nanstad, J. G. Merkle, W. R. Corwin, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack-Arrest Behavior in SEN Wide Plates of Quenched and Tempered A 533 Grade B Steel Tested Under Nonisothermal Conditions*, USNRC Report NUREG/CR-4930 (ORNL-6388), August 1987.
91. D. B. Barker, R. Chona, W. L. Fourney, and G. R. Irwin, University of Maryland, College Park, Md., *A Report on the Round Robin Program Conducted to Evaluate the Proposed ASTM Standard Test Method for Determining the Plane Strain Crack Arrest Fracture Toughness, K_{Ia} , of Ferritic Materials*, USNRC Report NUREG/CR-4966 (ORNL/Sub/79-7778/4), January 1988.
92. W. H. Bamford, Westinghouse Electric Corporation, Pittsburgh, Pa., *A Summary of Environmentally Assisted Crack-Growth Studies Performed at Westinghouse Electric Corporation Under Funding from the Heavy-Section Steel Technology Program*, USNRC Report NUREG/CR-5020 (ORNL/Sub/82-21598/1), May 1988.
93. R. H. Bryan, B. R. Bass, S. E. Bolt, J. W. Bryson, W. R. Corwin, J. G. Merkle, R. K. Nanstad, and G. C. Robinson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressurized-Thermal-Shock Test of 6-in.-Thick Pressure Vessels. PTSE-2: Investigation of Low Tearing Resistance and Warm Prestressing*, USNRC Report NUREG/CR-4888 (ORNL-6377), December 1987.
94. J. H. Giovanola and R. W. Klopp, SRI International, Menlo Park, Calif., *Viscoplastic Stress-Strain Characterization of A533B Class 1 Steel*, USNRC Report NUREG/CR-5066 (ORNL/Sub/87-SA193/1), September 1989.
95. L. F. Miller et al., Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Neutron Exposure Parameters for the Metallurgical Test Specimens in the Fifth Heavy-Section Steel Technology Irradiation Series Capsules*, USNRC Report NUREG/CR-5019 (ORNL/TM-10582), March 1988.
96. Canceled.
97. D. J. Naus, J. Keeney-Walker, and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *High-Temperature Crack-Arrest Behavior in 152-mm-Thick SEN Wide Plates of Quenched and Tempered A 533 Grade B Steel*, USNRC Report NUREG/CR-5330 (ORNL/TM-11083), April 1989.
98. K. V. Cook, R. A. Cunningham, Jr., and R. W. McClung, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Detection and Characterization of Indications in Segments of Reactor Pressure Vessels*, USNRC Report NUREG/CR-5322 (ORNL/TM-11072), August 1989.
99. R. D. Cheverton, W. E. Pennell, G. C. Robinson, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Impact of Radiation Embrittlement on Integrity of Pressure Vessel Supports for Two PWR Plants*, NUREG/CR-5320 (ORNL/TM-10966), February 1989.
100. D. J. Naus, J. Keeney-Walker, B. R. Bass, S. K. Iskander, R. J. Fields, R. deWitt, and S. R. Low III, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *SEN Wide-Plate Crack-Arrest Tests Utilizing A 533 Grade B Class 1 Material: WP-CE Test Series*, USNRC Report NUREG/CR-5408 (ORNL/TM-11269), November 1989.
101. D. J. Naus, J. Keeney-Walker, B. R. Bass, S. K. Iskander, R. J. Fields, R. deWitt, and S. R. Low III, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *High Temperature Crack-Arrest Tests Using 152-mm-Thick SEN Wide Plates of Low Upper-Shelf Base Material: Tests WP-2.2 and WP-2.6*, USNRC Report NUREG/CR-5450 (ORNL/TM-11352), February 1990.
102. Canceled.

103. D. J. Naus, J. Keeney-Walker, B. R. Bass, G. C. Robinson, S. K. Iskander, D. J. Alexander, R. J. Fields, R. deWitt, S. R. Low, C. W. Schwartz, and I.-B. Johansson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Crack-Arrest Behavior in SEN Wide Plates of Low Upper-Shelf Base Metal Tested Under Non-isothermal Conditions: WP-2 Series*, USNRC Report NUREG/CR-5451 (ORNL-6584), August 1990.
104. T. L. Dickson, R. D. Cheverton, and D. K. Shum, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Inclusion of Unstable Ductile Tearing and Extrapolated Crack-Arrest Toughness Data in PWR Vessel Integrity Assessment*, USNRC Report NUREG/CR-5473 (ORNL/TM-11450), May 1990.
105. T. J. Theiss, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Recommendations for the Shallow-Crack Fracture Toughness Testing Task Within the HSST Program*, USNRC Report NUREG/CR-5554 (ORNL/TM-11509), September 1990.
106. J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Overview of the Low Upper Shelf Toughness Safety Margin Issue*, USNRC Report NUREG/CR-5552 (ORNL/TM-11314), August 6, 1990.
107. D. K. M. Shum, J. G. Merkle, J. Keeney-Walker, and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Analytical Studies of Transverse Strain Effects on Fracture Toughness for Circumferentially Oriented Cracks*, USNRC Report NUREG/CR-5592 (ORNL/TM-11581), April 1991.
108. J. D. Landes, The University of Tennessee for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Extrapolation of the J-R Curve for Predicting Reactor Vessel Integrity*, USNRC Report NUREG/CR-5650 (ORNL/Sub/89-99732/1), January 1992.
109. J. Keeney-Walker, B. R. Bass, and J. D. Landes, (The University of Tennessee), Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *An Investigation of Crack-Tip Stress-Field Criteria for Predicting Cleavage-Crack Initiation*, USNRC Report NUREG/CR-5651 (ORNL/TM-11692), September 1991.
110. G. R. Irwin, University of Maryland, for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Use of Thickness Reduction to Estimate Values of K*, USNRC Report NUREG/CR-5697 (ORNL/Sub/79-7778/5), November 1991.
111. Canceled.
112. J. W. Dally, W. L. Fournery, and G. R. Irwin, University of Maryland for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Lower-Bound Initiation Toughness with a Modified-Charpy Specimen*, USNRC Report NUREG/CR-5703 (ORNL/Sub/79-7778/7), November 1991.
113. S. K. Iskander, G. C. Robinson, W. R. Corwin, B. C. Oland, D. J. Alexander, and K. V. Cook, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Experimental Results of Tests to Investigate Flaw Behavior of Mechanically Loaded Stainless Steel Clad Plates*, USNRC Report NUREG/CR-5785 (ORNL/TM-11950), April 1992.
114. S. T. Rolfe, University of Kansas for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Interpretive Report on the Application of Shallow-Flaw CTOD Test Data to the Structural Margin Assessment of Reactor Pressure Vessels with Flaws*, USNRC Report NUREG/CR-5767 (ORNL/Sub/90-SH640/1), November 1991.
115. D. E. McCabe, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Comparison of Weibull and β_{IC} Analysis of Transition Range Fracture Toughness Data*, USNRC Report NUREG/CR-5788 (ORNL/TM-11959), January 1992.
116. R. D. Cheverton, T. L. Dickson, J. G. Merkle, and R. K. Nanstad, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Review of Reactor Pressure Vessel Evaluation Report for Yankee Rowe Nuclear Power Station (YAEC No. 1735)*, USNRC Report NUREG/CR-5799 (ORNL/TM-11982), March 1992.
117. T. L. Dickson, R. D. Cheverton, and J. W. Bryson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Pressurized-Thermal-Shock Probabilistic Fracture Mechanics Sensitivity Analyses for Yankee Rowe Reactor Pressure Vessel*, USNRC Report NUREG/CR-5782 (ORNL/TM-11945), August 1993.
118. Canceled.

Prior

119. J. W. Dally, G. R. Irwin, X-J. Zhang, and R. J. Bonenberger, University of Maryland for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *The Influence of Precompression on the Lower-Bound Initiation Toughness of A 533 B Reactor Grade Steel*, USNRC Report NUREG/CR-5847 (ORNL/Sub/79-7778/8), May 1992.
120. Canceled.
121. Canceled.
122. A. R. Rosenfield and C. W. Marschall, Battelle Columbus Division for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Fracture-Mechanics-Based Failure Analysis*, USNRC Report NUREG/CR-5860 (ORNL/Sub/82-17651/1), June 1992.
123. G. R. Irwin and X-J. Zhang, University of Maryland for Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Gradient Study of a Large Weld Joining Two Forged A 508 Shells of the Midland Reactor Vessel*, USNRC Report NUREG/CR-5867 (ORNL/Sub/79-7778/10), June 1992.
124. J. Keeney-Walker and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *ORNOZL: A Finite-Element Mesh Generator for Nozzle-Cylinder Intersections Containing Inner-Corner Cracks*, USNRC Report NUREG/CR-5872 (ORNL/TM-11049), September 1992.
125. J. Keeney-Walker and B. R. Bass, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *A Comparison of Analysis Methodologies for Predicting Cleavage Arrest of a Deep Crack in a Reactor Pressure Vessel Subjected to Pressurized-Thermal-Shock Loading Conditions*, USNRC Report NUREG/CR-5793 (ORNL/TM-11969), September 1992.
126. T. J. Theiss, D. K. M. Shum, and S. T. Rolfe (University of Kansas), Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Experimental and Analytical Investigation of the Shallow-Flaw Effect in Reactor Pressure Vessels*, USNRC Report NUREG/CR-5886 (ORNL/TM-12115), July 1992.
127. B. R. Bass, D. K. M. Shum, and J. Keeney-Walker, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Constraint Effects on Fracture Toughness for Circumferentially Oriented Cracks in Reactor Pressure Vessels*, USNRC Report NUREG/CR-6008, (ORNL/TM-12131), August 1992.
128. R. E. Stoller, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Modeling the Influence of Irradiation Temperature and Displacement Rate on Radiation-Induced Hardening in Ferritic Steels*, USNRC Report NUREG/CR-5859 (ORNL/TM-12073), July 1992.
129. J. Keeney-Walker, J. G. Merkle, S. K. Iskander, and T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., Oak Ridge, Tenn., *Recommendations for Thermal-Shock Testing of Clad Cylinders with Shallow Surface Cracks*, USNRC Report NUREG/CR-5915 (ORNL/TM-12166), November 1992.
130. W. E. Pennell, B. R. Bass, R. K. Nanstad, J. G. Merkle, T. L. Dickson, T. J. Theiss, J. Keeney-Walker, and D. K. Shum, *Heavy-Section Steel Technology Program Semiannual Progress Report for October 1991 through March 1992*, NUREG/CR-4219 (ORNL/TM-9593/V9&N1) June 30, 1992.
131. W. E. Pennell, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "Heavy-Section Steel Technology Program: Recent Developments in Crack Initiation and Arrest Research," *Nineteenth Water Reactor Safety Information Meeting*, NUREG/CP-0119, Vol. 1, pp. 29-51, October 1992.
132. W. E. Pennell, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "Aging Impact on the Safety and Operability of Nuclear Reactor Pressure Vessels," *Proceedings of the Aging Research Information Conference*, USNRC Conference Proceeding NUREG/CP-0122, Vol. 1, pp. 431-453, March 1992.
133. T. L. Dickson and F. A. Simonen, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., "The Application of Probabilistic Fracture Analysis to Residual Life Evaluation of Embrittled Reactor Vessels," *Proceedings of the Aging Research Information Conference*, USNRC Conference Proceeding NUREG/CP-0122, Vol. 1, pp. 454-467, March 1992.
134. R. K. Nanstad, F. M. Haggag, D. E. McCabe, S. K. Iskander, K. O. Bowman, and B. H. Menke, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Irradiation Effects on Fracture Toughness of Two High-Copper Submerged-Arc Welds, HSSI Series 5*, USNRC Report NUREG/CR-5913, Vol. 1 (ORNL/TM-12156/V1) October 1992.

135. D. E. McCabe, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Evaluation of Crack Pop-ins and the Determination of Their Relevance to Design Consideration*, USNRC Report NUREG/CR-5952 (ORNL/TM-12247), February 1993.
136. Canceled.
137. R. K. Nanstad, D. E. McCabe, R. L. Swain, and M. K. Miller, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Chemical Composition and RT_{NDT} Determinations for Midland Weld WF-70*, USNRC Report NUREG/CR-5914 (ORNL-6740), December 1992.
138. T. J. Theiss, B. R. Bass, J. W. Bryson, W. J. McAfee, R. K. Nanstad, W. E. Pennell, and M. C. Rao, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Initial Results of the Influence of Biaxial Loading on Fracture Toughness*, USNRC Report NUREG/CR-6036 (ORNL/TM-12349), June 1993.
139. D. K. Shum, J. W. Bryson, and J. G. Merkle, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Potential Change in Flaw Geometry of an Initially Shallow Finite-Length Surface Flaw During a Pressurized Thermal Shock Transient*, NUREG/CR-5968 (ORNL/TM-12279), September 1993.
140. T. L. Dickson, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Generic Analyses for Evaluation of Low Charpy Upper-Shelf Energy Effects on Safety Margins Against Fracture of Reactor Pressure Vessel Materials*, USNRC Report NUREG/CR-6023 (ORNL/TM-12340), July 1993.
141. C. W. Schwartz, Department of Civil Engineering, University of Maryland, *Crack-Speed Relations Inferred from Large Single-Edge-Notched Specimens of A 533 B Steel*, NUREG/CR-5861 (ORNL/Sub/79-7778/9), July 1994.
142. B. R. Bass, J. W. Bryson, W. J. McAfee, T. J. Theiss, and M. C. Rao, Martin Marietta Energy Systems, Inc., Oak Ridge Natl. Lab., *Biaxial Loading and Shallow-Flaw Effects on Crack-Tip Constraint and Fracture Toughness*, NUREG/CR-6132 (ORNL/TM-12498), January 1994.
143. J. A. Keeney, B. R. Bass, W. J. McAfee, and S. K. Islander, *Preliminary Assessment of the Fracture Behavior of Weld Material in Full-Thickness Clad Beams*, USNRC Report NUREG/CR-6228 (ORNL/TM-12735), October 1994.
144. Canceled.
145. Canceled.
146. Canceled.
147. Canceled.
148. Robert H. Dodds, *Constraint Effects on Fracture Initiation Loads in HSST Wide-Plate Tests*, NUREG/CR-6259 (ORNL/TM-12796), July 1994.
149. G. R. Irwin and X. J. Zhang, *Cleavage Behaviors in Nuclear Vessel Steels*, USNRC Report NUREG/CR-6262 (ORNL/Sub/79-7778/11), November 1994.
150. W. J. McAfee, B. R. Bass, J. W. Bryson, and W. E. Pennell, *Biaxial Loading Effects on Fracture Toughness of Reactor Pressure Vessel Steel*, USNRC Report NUREG/CR-6273 (ORNL/TM-12866), November 1994.
151. D. E. McCabe, E. T. Mannes Schmidt, R. L. Swain, *Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials, Data Analysis*, NUREG/CR-6426 (ORNL/6892/V1), January 1997.
152. B. R. Bass, J. W. Bryson, T. L. Dickson, W. J. McAfee, and W. E. Pennell, "Development of a Shallow-Flaw Fracture Assessment Methodology for Nuclear Reactor Pressure Vessels," in *Life Assessment and Lift Extension of Engineering Plant, Structures and Components*, eds. J. H. Edwards, P. E. J. Flewitt, B. C. Gasper, K. A. McLarty, P. Stanley, B. Tomkins, *Proceedings of the Third International Conference on Engineering Structural Integrity Assessment*, Churchill College, UK, September 1996, Engineering Materials Advisory Services, 339 Halesowen Road, Cradley Heath, Warley, B64 6PH, UK, pp. 211-218.
153. J. A. Keeney, "Fracture Assessment of Weld Material from a Full-Thickness Clad RPV Shell Segment," presented at the 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996.
154. W. J. McAfee et al., "Biaxial Loading Effects on Shallow-Flaw Fracture Toughness in Reactor Pressure Vessel Steels," presented at the 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996.
155. D. E. McCabe and J. E. Merkle, "Estimation of Lower-Bound K_{Jc} on Pressure Vessel Steels from Invalid Data," presented at the 28th National Symposium on Fatigue and Fracture Mechanics, Saratoga Springs, New York, June 26-27, 1996.
156. J. G. Merkle, "Patterns and Perspectives in Applied Fracture Mechanics," *Fracture Mechanics*:

Prior

- Twenty-Sixth Volume, ASTM STP 1256*, W. G. Reuters, J. H. Underwood, and J. C. Newman, Jr., eds., American Society for Testing and Materials, Philadelphia, 1995, June 1996.
157. D. E. McCabe, "Overview of Proposed ASTM Test Method for the Determination of Reference Temperature T_{03} for Ferritic Steels in the Transition Range," presented at the ASTM Committee E08 Meeting, Orlando, Florida, May 20-23, 1996.
 158. W. E. Pennell, "Evaluating the Safety of Aging Nuclear Reactor Pressure Vessels," presented at the 58th Annual American Power Conference, Proceedings of the American Conference, Vol. 58, Chicago, Ill., April 9, 1996.
 159. W. E. Pennell, W. R. Corwin, and J. V. Pace III, "Structural Integrity Materials & Irradiation Effects Embrittlement Data Base & Dosimetry Evaluations," presented at a meeting to the joint DOE/NRC committee investigating Core Nuclear Capabilities at DOE National Laboratories, at White Flint-2, Washington, D.C., April 16, 1996.
 160. D. E. McCabe, E. T. Manneschildt, and R. L. Swain, "Ductile Fracture Toughness of Modified A 302 Grade B Plate Materials, Data Analysis," presented at the 58th Annual American Power Conference, Proceedings of the American Conference, Vol. 58, Chicago, Ill., April 9, 1996.