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ORNL-4816

HEAVY-SECTION STEEL TECHNOLOGY PROGRAM

*Semiannual Progress Report
Period Ending February 29, 1972*

MASTER

OAK RIDGE NATIONAL LABORATORY

OPERATED BY UNION CARBIDE CORPORATION • FOR THE U.S. ATOMIC ENERGY COMMISSION

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HEAVY-SECTION STEEL TECHNOLOGY PROGRAM
SEMIANNUAL PROGRESS REPORT
For Period Ending February 29, 1972

F. J. Witt, Program Director

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OCTOBER 1972

OAK RIDGE NATIONAL LABORATORY
Oak Ridge, Tennessee 37830
operated by
UNION CARBIDE CORPORATION
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U.S. ATOMIC ENERGY COMMISSION

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CONTENTS

	<u>Page</u>
FOREWORD	v
SUMMARY	vii
1. PROGRAM ADMINISTRATION AND PROCUREMENT	1
2. INVESTIGATIONS OF UNIRRADIATED MATERIALS	3
Material Characterization and Variability	3
Fracture Mechanics Investigations	9
Strain-Rate and Crack-Arrest Studies	9
Effects of Strain Gradients on the Gross Strain	
Crack Tolerance of A533 B Steel	10
Procedure	11
Experimental results	18
Discussion	24
Elastic-Plastic Behavior of a Longitudinal Semi-elliptical Crack in a Thick Pressure Shell	29
Mesh generation	30
Elastic analysis	33
Elastic-plastic analysis	35
Conclusions and recommendations for future work	38
Experimental Verification of Lower-Bound K_{Ic} Values	
Utilizing the Equivalent-Energy Concept	40
Effect of various relative displacement gage lengths on K_{Icd} measurements	40
Lower-bound K_{Ic} testing	42
Experimental results	43
Static and Dynamic Fracture Toughness Tests on Precracked Charpy Specimens	47
Fatigue-Crack-Growth Study of Nuclear Pressure Vessel Materials in Simulated Reactor Environment	56
Effect of Some Experimental Parameters on Crack-Growth	
Incubation in Corrosion Environment	57
Crack Propagation Rate as a Function of Stress Intensity Factor Range, ΔK_I	59
Summary and Conclusions	63
3. INVESTIGATIONS OF IRRADIATED MATERIALS	65
Irradiation Effects on the Fracture of Heavy-Section Pressure Vessel Steels	65
Objective	65
Effect of Strain Rate on the Tensile Behavior of ASTM A533 B	66
Fatigue-Crack Propagation of Irradiated ASTM A533 B	68

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Testing of Compact-Tension Specimens for Lower-Bound Toughness K_{Ic}	71
Lower-Bound Toughness of Irradiated A533, Grade B, Class 1 Steel	71
Erratum, Radiation Hardening and Embrittlement in ASTM A533, Grade B, Class 1 Steel	75
Neutron Irradiation Embrittlement of ASTM A533 B Steel Irradiated at 300 and 400°C to Fast-Neutron Fluences of 1, 5, and 10×10^{19} neutrons/cm ²	76
Extent of VDEH Irradiation Program	76
HSST Reference Steel, ASTM A533, Grade B Irradiations	78
4. PRESSURE VESSEL AND PIPING INVESTIGATIONS	85
Testing of 1- and 6-in.-thick Flawed Tensile Specimens	85
One-Inch-Thick Flawed Tensile Specimens	85
Six-Inch-Thick Flawed Tensile Specimens	89
Intermediate Vessel Test Support Activities	89
Strain Gage Developments	89
Crack Sharpening	93
Procurement of Intermediate Test Vessels	96
Ductile Pipe Rupture Research Program	99

FOREWORD

The Heavy-Section Steel Technology (HSST) Program is a USAEC-sponsored effort for investigating the effects of flaws, variations of properties, stress raisers, and residual stress on the structural reliability of present and contemplated water-cooled reactor pressure vessels. The cognizant engineer for USAEC is J. R. Hunter. At ORNL the program is under the Pressure Vessel Technology Program, of which G. D. Whitman is Director. The HSST program is being carried out in very close cooperation with the nuclear power industry. Prior reports in this series are ORNL-4176, ORNL-4315, ORNL-4377, ORNL-4463, ORNL-4512, ORNL-4590, ORNL-4653, ORNL-4681, and ORNL-4764.

SUMMARY

The Heavy-Section Steel Technology (HSST) Program is one of the major USAEC-sponsored efforts concerned with the primary vessels for civilian water-reactor power plants. The main emphasis of the program is on the effects of flaws, material inhomogeneities, and discontinuities on the behavior of the vessels under normal operating and accident conditions. The program is coordinated with efforts by the manufacturing and utility sectors of the nuclear power industry to assess the in-service structural safety of the massive pressure vessels typical of those used in boiling- and pressurized-water reactor systems. Particular emphasis is placed on developing a technology that can be used to establish the margin of safety against failure of the vessels, particularly catastrophic failure beyond the control of plant safety features.

There are 12 tasks of the HSST program. In this report the activities of these tasks are grouped under administration and procurement, investigations of unirradiated materials, investigations of irradiated materials, and pressure vessel and piping investigations.

There are 9 active research and development subcontracts sponsored by the HSST program. Results from these efforts as well as those performed at ORNL are extensively reported in technical reports and publications.

Variability studies continued in which various mechanical property data, yield strengths, ultimate strength, and elongation and reduction-in-area values are compared as a function of plate section, plate location, depth location, and orientation. Charpy V-notch impact data are also compared as functions of position and orientation.

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Materials Research Laboratory is now extending previous efforts on characterizing crack-arrest toughness to determine its relationship with the lower-bound arrest values obtained for smaller specimens.

TRW Systems completed the third year's work on the study of the ability of a material to tolerate strain in the presence of a flaw as a function of flaw size and temperature by the testing of beam specimens in four-point bending. It was suggested that the total energy concept applied to such structures which have both compressive and tensile strain gradients is not sufficient alone to permit prediction of behavior.

In studying the elastic-plastic behavior of a longitudinal semielliptical crack in a thick pressure vessel by means of finite-element analyses, Brown University formed a crack-tip element from an eight-node isoparametric element by coalescing pairs of nodes around the crack boundary. Meshes generated in this way gave good results for stress intensity factors, even for coarse meshes. Results were obtained for a semielliptical crack in tensile specimens and in a thick-walled cylinder, and an elastic-plastic analysis of the flawed thick-walled cylinder was performed.

Westinghouse Nuclear Energy Systems evaluated a high-strength rotor-forging-grade material (NiCrMoV), which was previously demonstrated to exhibit a K_{Ic} upper shelf with 8-in.-thick (8T) compact-tension (CT) specimens, utilizing the equivalent-energy concept. The specimens were tested in the temperature range 0 to 550°F, and the results were analyzed using Witt's equivalent-energy concept.

Testing of 0.394T CT (C_v thickness) and 3T CT specimens fabricated from A533, grade B, class 1 steel was completed and the fracture toughness parameter K_{Icd} determined.

A 1T CT specimen was tested to determine experimentally the effect of various displacement gage measuring geometries on the K_{Icd} measurements. The results indicated that there is no distinguishable variation or trend which can be attributed to the different gage lengths and gage positions employed.

A study of static and dynamic toughness of the Charpy-type specimen to determine the potential of the fracture toughness parameters K_{Icd} and K_{Idd} continued.

The JAERI-sponsored study of fatigue-crack growth in nuclear pressure vessel materials in a simulated reactor environment has disclosed that crack growth is incubated under conditions favorable to pitting corrosion and that load-controlled vs displacement-controlled fatigue tests display an apparent discrepancy as analyzed by means of the fracture mechanics correlation.

Hanford Engineering Development Laboratory (HEDL) evaluated unirradiated tensile yield strengths of A533 B from HSST plate 02 at strain rates from 10^{-5} to 10^2 sec^{-1} in the temperature range -319 to 500°F . The results were correlated with a rate-temperature parameter which will serve as a valid basis for more extensive interpretation of studies of the irradiation effect on the strain rate sensitivity of yield strength.

The preliminary results of specimens fatigue cycled at room temperature tend to indicate that, at the lower values of ΔK , the fatigue-crack growth rate of A533 steel irradiated to $2.3\text{--}2.8 \times 10^{19}$ neutrons/cm² is somewhat higher than corresponding behavior of unirradiated material.

Also, series of longitudinally and transversely oriented Charpy-specimen-thickness (0.394-in.) CT specimens were machined by ORNL from broken halves of irradiated 1T CT specimens. These specimens were tested at HEDL. Lower-bound toughness K_{IC} values of about $100,000 \text{ psi}\sqrt{\text{in.}}$ were shown to exist for both orientations at 200°F for the irradiation condition (nominally 2×10^{19} neutrons/cm², $E > 1 \text{ MeV}$, irradiated at 540°F) investigated. Values ranging up to $166,000 \text{ psi}\sqrt{\text{in.}}$ were found at higher temperatures. These results are the first obtained which demonstrate the high toughness of the steel even when embrittled by irradiation.

A description is presented of the Verein Deutscher Eisenhüttenleute (VDEh) sponsored neutron irradiation embrittlement study of ASTM A533, grade B, class 1 steel irradiated at 300 and 400°C to fast-neutron fluences of 1, 5, and 10×10^{19} neutrons/cm² to be performed by Kernforschungsanlage Jülich GmbH. Specimens from two sections of HSST plates 02 and 03 will be irradiated as reference material for comparison of simultaneous irradiations of several low-alloy MnNiMoV and NiCrMo steels.

Southwest Research Institute continued the testing of the 1/6-scale models of the 6-in.-thick tensile specimens. A summary of the data from all 1-in.-thick specimens obtained to date is presented.

Preparations for the testing of the intermediate test vessels continued, with particular emphasis on development of techniques for strain gaging of the interior of the vessels and for controlled propagation and monitoring of fatigue cracks in the vessels.

Progress was made in the elimination of fabrication problems on the intermediate test vessels such that three vessels were completed and final testing, inspection, and acceptance performed.

A program is being initiated at Battelle Columbus Laboratory to investigate further the stress-state conditions which lead to tearing shear fractures.

I. PROGRAM ADMINISTRATION AND PROCUREMENT

The Heavy-Section Steel Technology (HSST) Program is one of the major USAEC-sponsored engineering research efforts concerned with the safety of civilian water-reactor pressure vessels. The main thrust is placed on the effects of flaws, material inhomogeneities, and discontinuities on such pressure vessels under normal operating and accident conditions. The areas of research interest cover the gamut of materials technology including irradiation effects. Major emphasis is placed on developing a technology that can be used to establish the margins of safety against failure of the vessels, particularly catastrophic vessel failure beyond the control of design safety features.

The HSST program is coordinated with efforts by other government agencies and the manufacturing and utility sectors of the nuclear power industry both in the United States and abroad. These total efforts should result in the quantification of safety assessments which are needed by the USAEC regulatory bodies, the professional code-writing bodies, and the nuclear power industry.

The activities of the HSST program are carried out under 12 separate tasks. About 75% of the task activities are performed under subcontract by research facilities throughout the United States and through informal cooperative efforts abroad. Currently there are 9 research and development subcontracts in force. In addition, a panel of 28 scientists and engineers with expertise in materials technology has been organized to serve in an advisory capacity to the program.

Administratively, the HSST program is carried out as task activities; however, for reporting purposes here, the research areas are discussed

under investigations of unirradiated materials, investigations of irradiated materials, and pressure vessel and piping investigations. The major procurement items are the intermediate test vessels; the status of the procurement of the vessels is summarized in Chapter 4.

2. INVESTIGATIONS OF UNIRRADIATED MATERIALS

The materials investigations under the HSST Program are divided, in general, into studies of unirradiated materials and studies of irradiation effects. The studies of unirradiated materials include inspection, characterization, variability determinations, transition temperature investigations, fracture mechanics studies, and fatigue-crack propagation tests. The current work in these areas is discussed in this section. No work specifically under the transition temperature task is in progress. Additional results for unirradiated materials are presented in Chapter 3, and inspection activities are reported in Chapter 4.

MATERIAL CHARACTERIZATION AND VARIABILITY

W. J. Stelzman

Tensile test results have been obtained from miniature 0.178-in.-gage-diam specimens taken from various depths and orientations in HSST plates 02 and 03 at the locations shown in Fig. 2.1 and listed in Tables 2.1 and 2.2. The areas sampled were 02AH, 02F, 02HC, 03CA, 03CB, and 03GA. Tensile data from areas 02FB and 03E^{1,2} and plate 01³ have been published.

¹R. G. Berggren, *HSST Program Semiannu. Progr. Rep. Aug. 31, 1968*, ORNL-4377, pp. 20-29.

²R. G. Berggren, *HSST Program Semiannu. Progr. Rep. Feb. 28, 1969*, ORNL-4463, pp. 20-29.

³R. G. Berggren et al., *HSST Program Semiannu. Progr. Rep. Feb. 28, 1971*, ORNL-4681, pp. 3-6.

Table 2.1. Tensile properties of 12-in.-thick BSST plate,^a
ASTM A533, grade B, class 1 steel

Plate section ^b	Depth in plate (d/12)	Orientation ^c	Strength properties (ksi)			Elongation (%)		Reduction in area (%)
			Upper yield	Lower yield	Ultimate tensile ^d	Uniform	Total ^e	
02AH	0	R	83.8	81.5	96.9	9.5	19.5	71.0
02AH	0	W	84.0	81.0	96.4	10.0	19.4	70.1
02F	0	R	82.7	80.9	96.5	10.6	21.1	71.7
02F	0	W	84.2	79.6	97.1	9.3	18.9	69.5
02HC	0	R	85.6	80.2	95.8	10.6	20.8	72.3
02HC	0	W	81.7	80.3	95.8	10.8	20.1	69.0
03CA	0	R		78.9	95.4	10.0	19.3	73.3
03CA	0	W		78.8	95.3	11.4	20.1	69.6
03CB	0	R	77.8	75.4	91.9	11.1	21.5	73.1
03CB	0	W		75.7	92.6	10.5	20.3	70.3
03GA	0	R	77.7	75.2	95.6	10.6	19.6	72.7
03GA	0	W	79.0	74.6	91.4	10.3	19.2	70.4
02AH	1/4	R	69.9	68.7	90.3	10.2	19.9	77.4
02AH	1/4	W	69.4	68.5	90.3	9.2	17.5	63.3
02F	1/4	R	70.2	69.6	91.7	9.5	17.7	67.9
02F	1/4	W	71.0	69.9	91.3	10.2	16.9	56.3
02HC	1/4	R	65.8	65.3	85.9	11.4	20.4	70.4
02HC	1/4	W	65.3	64.3	85.1	10.3	20.5	68.2
03CA	1/4	R		65.0	88.2	9.2	18.2	70.0
03CA	1/4	W		65.7	89.3	8.9	16.6	65.6
03CB	1/4	R		65.1	87.5	9.8	20.3	70.6
03CB	1/4	W		63.7	86.7	9.2	17.3	67.0
03GA	1/4	R	65.1	64.5	86.0	9.5	18.4	69.1
03GA	1/4	W		62.0	83.9	10.6	19.2	67.3
02HC	1/2	R	63.3	61.8	82.4	10.8	21.4	72.1
02HC	1/2	W	64.2	63.1	83.2	9.9	17.8	66.3
02HC	3/4	R	66.0	64.6	85.8	10.6	20.2	68.5
02HC	3/4	W		64.4	85.1	10.3	19.7	66.8
02HC	1	R	78.0	73.7	89.9	12.0	20.1	69.6
02HC	1	W	76.1	73.6	90.1	12.0	21.3	66.2

^a0.178-in.-gage-diam specimens tested at 75°F and a strain rate of 0.016 in. in.⁻¹ min.⁻¹.

^bRefers to plate section and location in Fig. 2.1.

^cSpecimen axial dimension in R (major rolling direction) or W (transverse to major rolling direction of plate).

^dDefined as the maximum load after the yield point divided by the original area of cross section.

^eLength-to-diameter ratio: 7.

Fig. 2.1. Test locations in HSST plates 02 and 03.

At the surface level of the plate areas sampled, the yield strengths appear to be uniform over plate 02 but vary somewhat over the upper portion of plate 03. This situation is reversed at the $1/4T$ level, where plate 03 shows a uniform distribution of strength and plate 02 data indicate lower values for the lower end of the plate. These may be local variations; however, neither plate has indicated abnormally low areas as did the 01SH region.³ Plate 02 does show a general trend toward higher tensile strength values than plates 01 and 03, which appear to be similar.

The through-thickness tensile sampling at location 02HC may be compared with previous through-thickness sampling at locations 01K and 03E.² Strength and elongation values show the same type of distribution, although the strength values for plate 02 appear to be somewhat lower through the central thickness portion of the plate. The elongation values would also appear to be lower for plate 02; however, the gage-length to gage-diameter ratio for the plate 02 specimens is 7:1, while the ratio for the 01K and 03E specimens was 4:1. This would account for a considerable portion of the decrease. The greatest difference appears to be in the reduction-of-area results, where plates 01 and 03 show much lower values

Table 2.2. Variation of yield stress^a in
12-in.-thick HSST plate, ASTM A533,
grade B, class 1 steel

Plate section ^b	Orienta- tion ^c	Depth in plate (ksi)					
		0T	1T	1/4T	3/4T	3/8T	1/2T
		$\times 10^3$	$\times 10^3$	$\times 10^3$	$\times 10^3$	$\times 10^3$	$\times 10^3$
02AH	R	81.5		68.7			
	W	81.0		68.5			
02F	R	80.9		69.6			
	W	79.6		69.9			
02FB	R					69.8	
	W						
02HC	R	80.2	73.7	65.3	64.6		61.8
	W	80.3	73.6	64.3	64.4		63.1
03CA	R	78.9		65.9			
	W	78.8		65.7			
03CB	R	75.4		65.1			
	W	75.7		63.7			
03GA	R	75.2		64.5			
	W	74.6		62.0			
03E	R	79.0 ^d	76.4 ^d	67.1 ^d	66.5 ^d		65.7 ^d
	W	78.4 ^d		66.5 ^d			64.7 ^d

^a0.178-in.-gage-diam specimens tested at 75°F and a strain rate of 0.016 in. in.⁻¹ min⁻¹.

^bRefers to plate section and location in Fig. 2.1.

^cSpecimen axial dimension in R (major rolling direction) or W (transverse to major rolling direction of plate).

^dAverages include standard 0.505-in.-diam 2-in.-gage-length specimen data.

for the central-thickness portion than plate 02. This is probably due to the combined effect of the high inclusion content of the steel at mid-thickness and the fact that the specimen cross section of the 01 and 03 specimens is eight times greater than that of the 02 specimens. Also, the strength and elongation data from all three plates show little sensitivity to specimen orientation parallel to the plate surface. The orientation has had a noticeable effect on the reduction-in-area values, where

the longitudinal (R) orientation provides higher values than the transverse (W) direction.

Charpy V-notch impact data were also obtained at the aforementioned plate locations and are summarized in Table 2.3. Plate 02 generally provided lower transition temperatures at the surface and 1/4T levels than plates 01 and 03 when compared at the 30 ft-lb correlation energy level. Comparison of the upper shelf energies is difficult due to the inherent scatter in these values. However, data from the longitudinal (RW) specimens indicate lower transition temperatures and higher shelf energies than those from the transverse (WR) specimens from all three plates.

Figure 2.2 presents a correction of the fracture energy parameters determined in instrumented Charpy tests on HSST submerged-arc weldment reported in the last HSST progress report.⁴

⁴W. J. Stelzman, *HSST Program Semiannu. Progr. Rep. Aug. 31, 1971*, ORNL-4764, p. 4.

Fig. 2.2. Effect of temperature on energy parameters determined in instrumented Charpy tests on HSST submerged-arc weldment 51B; WL-oriented specimens.

Table 2.3. Variation of Charpy V-notch impact properties of 12-in.-thick HSST plate, ASTM A533, grade B, class 1 steel

Plate section ^a	Orienta- tion ^b	Depth in plate					
		0T	1T	1/4T	3/4T	3/8T	1/2T
Transition temperature at 30 ft-lb (°F)							
02AH	RW	-155		0			
02AH	WR	-115		10			
02F	RW	-165		-5			
02F	WR	-125		10			
02FB	RW	-125		20		40	30
02FB	WR	-100		20			55
02HC	RW	-150	-140	-5	0		0
02HC	WR	-115	-110	20	15		0
03CA	RW	-150		15			
03CA	WR	-130		30			
03CB	RW	-165		20			
03CB	WR	-135		25			
03GA	RW	-135		30			
03GA	WR	-110		30			
03E	RW	-130	-115	30	25		40
03E	WR	-95	-105	40	30		50
Shelf energy (ft-lb)							
02AH	RW	115		110			
02AH	WR	94		96			
02F	RW	117		106			
02F	WR	96		95			
02FB	RW	105		105		115	115
02FB	WR	85		97			90
02HC	RW	122	118	127	120		115
02HC	WR	89	96	100	107		102
03CA	RW	111		109			
03CA	WR	82		95			
03CB	RW	111		111			
03CB	WR	91		95			
03GA	RW	115		119			
03GA	WR	85		96			
03E	RW	118	118	118	118		124
03E	WR	88	84	92	96		87

^aRefers to plate section and location in Fig. 2.1.

^bFirst letter indicates orientation of axial center line of specimen and second letter indicates fracture propagation direction; R and W refer to principal rolling direction and width direction of plate respectively.

FRACTURE MECHANICS INVESTIGATIONS

J. G. Merkle

The objectives of the fracture mechanics investigations are (1) to establish the applicability of fracture mechanics methods as currently understood to the low-alloy steels being used in reactor pressure vessels, (2) to extend the applicability of this knowledge to cover the tougher behavior at higher temperatures, and (3) to develop new methods specifically applicable for predicting the fracture behavior of steels that undergo gross plastic strains before fracture. These objectives are being pursued through a series of subcontracts, with related tasks (described elsewhere) contributing significantly to the developments. The activities under the subcontracts are discussed here.

Strain-Rate and Crack-Arrest Studies⁵

P. B. Crosley E. J. Ripling
Materials Research Laboratory

The program at Materials Research Laboratory has been primarily concerned with crack-arrest toughness K_{Ia} , measured with contoured double-cantilever beam (DCB) specimens. In recent work single-edge notch toughness specimens have been tested in order to demonstrate that the K_{Ia} value characterizes crack-arrest conditions in structures where the stress intensity factor increases as the crack extends. Tests of this type have been successfully carried out at room temperature.

An extension of this work now centers on the question of where the crack-arrest toughness K_{Ia} fits into the scheme for calculating lower-bound K_{Ic} numbers developed by Witt et al.⁶

The experimental work involves the testing of 24 three-point bend specimens. Six specimens will have a thickness of 1.5 in. and other

⁵Work being performed under UCCND Subcontract No. 3152 between Union Carbide Corporation and Materials Research Laboratory.

⁶F. J. Witt and T. R. Mager, *A Procedure for Determining Bounding Values on Fracture Toughness K_{Ic} at Any Temperature*, Fifth National Symposium on Fracture Mechanics, University of Illinois, Urbana, Ill., Aug. 31-Sept. 2, 1971. Also ORNL-TM-3894 (in publication).

dimensions scaled in accordance with ASTM Standard E399-70T. Eighteen specimens will be 0.5 in. thick and geometrically similar to the thicker specimens.

The specimens will be machined from HSE material already on hand at Materials Research Laboratory. The specimens will be fatigue cracked in accordance with ASTM Standard E399-70T. It is hoped that the thicker specimens can be tested over a range of temperatures from 70 to 200°F; however, the actual test temperatures will be selected on the basis of the test results as they are obtained. The objective is to conduct tests at temperatures where pop-in behavior occurred without complete failure of the specimen.

The thinner specimens will be tested at the highest and lowest temperatures used in testing the larger ones. Tests will be made on specimens having a relative crack length a/W equal to that of the arrested crack in the 1.5-in.-thick specimens. In addition, tests will be made on specimens with crack lengths of different values. The tests on the smaller specimens will not be fracture toughness tests per se; their purpose is to establish the $P-\Delta$ curve to be used in interpreting the results of the tests on the thicker specimens.

Effects of Strain Gradients on the Gross Strain
Crack Tolerance of A533 B Steel⁷

P. N. Randall
TRW Systems

Most structural designs intend that general yielding should always precede fracture, even in the presence of the largest expected flaw. Therefore, to evaluate the margin of safety against fracture the designer needs test data for his material in terms of its tolerance for strain in the presence of a flaw as a function of flaw size and temperature. This report gives the third year's results of our effort to develop a test for gross strain crack tolerance.

⁷Work performed under UCCND Subcontract No. 313¹ between Union Carbide Corporation and TRW Systems, Inc.

The reasons for studying the effects of strain gradients on gross strain crack tolerance are twofold. Previous test data^{8,9} have been obtained in axial tension, but the typical service application is a region of strain concentration, with gross strain gradients in the direction of crack advance and also along the normal to the crack plane. The thick-walled pressure vessel also has a strain gradient through the membrane wall. Thus an understanding of strain-gradient effects is needed to guide the application of test data to design.

The second reason for this study is to broaden our understanding of the gross strain concept. Most would agree that crack propagation is caused by the local, crack-tip conditions of stress and strain, but no one knows how to measure them. Hence the gross strain measurements made in the program were considered to be related to local conditions in two ways: (1) as the driving force that governs the magnitude of local strain and (2) as the measure of relief of constraint through the extension of plastic zones to free surfaces.

The test plan was confined to one size of surface crack (0.22 in. deep), the middle size of the three tested previously.⁸ After rechecking the test results for loading in uniform tension, four series of bend tests were run, plus three series where the crack was placed in the bottom of a groove in the face of a tension or bend specimen. Strains measured at various locations on the specimens were analyzed to determine those that gave the most consistent explanations of results for all specimen geometries.

Procedure

Material. All test specimens were taken from plate 02, one of the 12-in.-thick plates that was especially rolled from A533 B steel for the

⁸P. N. Randall, *Gross Strain Measure of Fracture Toughness of Steels*, HSSTP-TR-3 (Nov. 1, 1969) or P. N. Randall and J. G. Merkle, same title, *Nucl. Eng. Des.* 17(1), 46-63 (1971).

⁹P. N. Randall, *Gross Strain Crack Tolerance of A 533-B Steel*, HSSTP-TR-14 (May 1, 1971) or P. N. Randall and J. G. Merkle, same title, *ASME Journal of Basic Engineering* (to be published), presented as Paper 71-WA/PVP-6.

HSST program. To avoid surface effects such as those from varying cooling rate, only midthickness material 3.5 in. or more from either plate surface was used. Orientation of the specimens was such that the material was stressed in the rolling direction and the direction of crack advance was through the plate thickness. Room-temperature values of yield and ultimate strength were 72 and 92 ksi respectively. Detailed information is found in Refs. 8-10.

Specimen and loading configurations. Transverse strain gradients were studied with beam specimens loaded in four-point bending. The specimen proper was 0.60 in. thick, 2.64 in. wide, and 22 in. long, and had a semielliptical surface crack 0.68 in. long by 0.22 in. deep at midspan on one face. Cracks were grown in bending fatigue, with a stress cycle of +4 to +39 ksi, once the crack appeared at the end of the ground slot used for a crack starter.

Three degrees of severity of transverse strain gradient were tested, as shown in Fig. 2.3, series 1, 2, and 3. Beam depth was varied by welding on a backup bar of the material, in the manner illustrated in Fig. 2.3, so the specimen proper would function as a cover plate for the built-up beam. The welds did not extend into the gage length, because there is no shear to transmit between the specimen and backup bar in the midspan region between the loading points.

In the first three test series, strain decreased in the direction of crack advance. In series 4, strain was made to increase in the direction of crack advance by welding the backup bar to the cracked face as shown in Fig. 2.3. This configuration simulates a crack in the inner surface of a heavy-walled vessel.

Axial strain gradients combined with transverse gradients were studied by means of specimens in which the crack was placed in the bottom of a groove. For series 5, we used a double-groove tensile specimen, 0.75 in. thick by 2.64 in. wide, bearing round-bottomed grooves on both faces as shown in Fig. 2.4. The net section was 0.60 in. thick, the same as for

¹⁰P. N. Randall, *Effects of Strain Gradients on the Gross Strain Crack Tolerance of A 533-B Steel*, HSSTP-TR-19. Obtain copies from Laboratory Records Department, Oak Ridge National Laboratory, P.O. Box X, Oak Ridge, Tenn. 37830.

Fig. 2.3. Beam cross sections used to produce transverse strain gradients.

SERIES 5 - DOUBLE - GROOVE SPECIMEN

SERIES 6 - DOUBLE - GROOVE SPECIMEN TESTED IN TENSION

SERIES 7 - DOUBLE - GROOVE SPECIMEN TESTED IN BENDING

Fig. 2.4. Configuration of the double-groove tension specimen for series 5 and the single-groove specimen for series 6, tension, and series 7, bending.

series 1 to 4. Mild notch effects (K_t was 1.47) were applied in this series in order to permit a better interpretation of the net section effect. For series 6, we used a single-groove tensile specimen, also shown in Fig. 2.4, for which K_t was 1.68. Transverse strain gradients were greater in this case. Finally, for series 7 we used the same configuration as for series 6, but loaded it in bending (K_t was approximately 1.44).

The cracks grown in the grooves were somewhat shallow relative to their length. Since we chose to keep crack depth constant throughout, the specimens were refatigued to a crack length of 0.80 in. for series 5 and 0.90 in. for series 6 and 7.

Displacement measurements. Techniques used earlier⁹ for the tension series were continued in this work, with some modification of the extensometers and the method of attachment to the specimen necessitated by the curvature developed in bending. Table 2.4 defines the strain values to be discussed by describing the displacement measurements on which they are based. The extensometer system is described therein by reference to Fig. 2.5. Posttest micrometer readings of width and thickness reduction were also used. The resulting reduction-of-area (R.A.) value was converted to a value of total longitudinal strain by the expression $\epsilon = 0.25 + \text{R.A.}/(1 - \text{R.A.})$, where the elastic portion was taken to be that at yield, nominally 0.25%.

For series 1 to 3, five records were obtained from the seven extensometers. Load was recorded as the abscissa on three x - y or x - yy plotters. Displacements ϵ_{gn} , cracked face, and ϵ_{gn} , back face, are recorded on one plotter, and ϵ_{gg} values (average of left and right) on another. A third single-pen plotter recorded crack opening displacement (COD) vs load. For series 4 through 7, there were only four records.

The critical values of the strains derived from these measurements were those at maximum load. In most tests, fracture occurred on rising load, which makes this definition of critical strain very clear-cut. At the highest test temperature in each series, however, the strain tolerance was such that load passed through a maximum for other causes. In tension, the cause was tensile instability, wherein the important material property was the strain-hardening coefficient and the significant geometric change was the reduction of area. In bending, series 1 to 4, however, when the

Table 2.4. Notes on displacement measurements

	Strain value	Displacements measured	Method	Gage length (in.)	Location of gage length	Gage points
All series	ϵ_{gnc} from R.A. COD	Width and thickness reduction Separation of adapters spot welded to crack face	Micrometer (post-test) Extensometer	0.2	0.2 in. back from fracture Centered on crack at specimen surface	Spot welds
Series 1 to 4 (see Fig. 2.5)	ϵ_{gnc} , cracked face ϵ_{gnc} , back face	Separation of adapters on gage blocks, top and bottom (see text)	Extensometer	3.2	Centered on crack plane	Fillet welds on gage blocks
	ϵ_{ggc} , cracked face ϵ_{ggc} , back face	Separation of horizontal pins on gage blocks top and bottom	2 extensometers, average of left and right	1.0	From 1.6 to 2.6 in. from crack plane, left and right	Fillet welds on gage blocks
	$\epsilon_{crack-tip}$ depth $\epsilon_{midcrack}$ depth	Obtained by analysis from ϵ_{gnc} , cracked face, and ϵ_{gnc} , back face	Linear interpolation	3.2	0.22 and 0.11 in., respectively, below surface of cracked face, centered on crack plane	
Series 5 and 6 (see Fig. 2.5)	ϵ_{groove}	Separation of adapters spot welded to surface of groove on cracked face	2 extensometers, average of left and right	0.2	0.5 in. in from edge of specimen	Spot welds
	ϵ_{back} face	Separation of adapters spot welded to back face	Extensometer	1.2	On back face, spanning the groove	Spot welds
	ϵ_{ggc} , cracked face ϵ_{ggc} , back face	Separation of gage holes	Hamilton inside micrometer, modified	0.5	From 1.0 to 1.5 in. from crack plane	0.062-in.-diam hole
	$\epsilon_{crack-tip}$ depth $\epsilon_{midcrack}$ depth	Obtained by semigraphical analysis (see text)				
Series 7 (see Fig. 2.5)	ϵ_{groove}	Same as series 5 and 6				
	ϵ_{back} face	Separation of pins on gage blocks top and bottom	4 extensometers, average 2 top and 2 bottom	1.2	Gage blocks welded to both edges of specimen	Fillet welds on gage blocks
	$\epsilon_{crack-tip}$ depth $\epsilon_{midcrack}$ depth	Obtained by semigraphical analysis (see text)

Fig. 2.5. Typical views of extensometer system.

deflection was large the reaction of the outer roller supports rotated inward from the vertical. This change in the geometry of loading eventually produced a maximum in the load. Its occurrence was so dependent on beam depth and span that maximum load was not significant in the bend tests unless caused by fracture.

Reduction of the displacement data for series 1 to 4 to obtain ϵ_{gnc} required some analysis, because a ductile specimen produced a large amount of relative rotation of the gage blocks. Derivations of the expressions used to obtain surface strains ϵ_{gnc} from the recorded displacements are given in Ref. 10.

Experimental results

Uniform tension. The base line for comparison of strain gradient effects was the tension data for the 0.22-in.-deep crack, reported previously,⁹ supplemented by some additional tests. The results, plotted in Fig. 2.6, show a consistent pattern of transition behavior for the displacement values ϵ_{gnc} , ϵ_{ggc} , and COD with the foot of the transition at -20°F . The curve labeled " ϵ_{gnc} from R.A." (gross strain in the net section obtained from reduction-of-area measurements taken 0.2 in. back from the fracture) was included to indicate the degree of heterogeneity of strain within the 3.2-in. gage length spanning the net section. In later comparisons of the results for all eight series, more consistent results were obtained by using " ϵ_{gnc} from R.A." As a point of interest, the reduction of width was nearly equal to the reduction of thickness.

Strain gradient effects. For series 1 (neutral axis at midthickness) and series 2 (neutral axis at back face), the logarithm of critical strain and COD values is plotted as a function of test temperature in Fig. 2.7, with the tension data added for reference. Width of the cross-hatched band between the plots for strain in the net section at the cracked face and at the crack-tip depth is a measure of the extent of the strain gradient. Note how gradually the strain tolerance develops with rising temperature compared with the tension test results. Another significant difference is the proximity of ϵ_{ggc} to ϵ_{gnc} in the bend series; that is, there was little net section effect compared with that seen in tension. The visual appearance of the bend specimens confirmed this in that the

Fig. 2.6. Critical strain and COD values as a function of temperature for surface-cracked specimens tested in tension.

Fig. 2.7. Critical strain and COD values as a function of temperature for series 1, $\epsilon_{\text{back face}}/\epsilon_{\text{cracked face}} = -1.0$, and for series 2, $\epsilon_{\text{back face}}/\epsilon_{\text{cracked face}} = 0$, compared with tension series (dashed lines).

curvature seemed to be uniform throughout the central 6 in. between loading points without a visible kink at the cracked section.

Series 3 and 4, plotted in Fig. 2.8, were also bend specimens having the neutral axis 0.6 in. above the back face of the specimen proper, the difference being that the crack advanced into an increasing strain field in series 4. Development of the transition in series 3 was far less abrupt than in series 4. It would seem logical for the tension data to fall in between those for series 3 and 4, but they did not. The slope was right, but there was a 40° upward shift in the foot of the transition. Our explanation for this rests on the difference in the manner of loading. Unlike the tension specimen, series 3 and 4 specimens were forced to deform equally in the gross and net sections. This can be seen in Fig. 2.8, which also reveals that strains in the gross section on the cracked face in series 3 and on the other face in series 4 were in the plastic range even at the lowest temperatures. Relief of constraint caused by such gross-section yielding is known from experience to promote higher values of gross strain crack tolerance (GSCT).

Series 5 (double groove) and series 6 (single groove) were both tested in tension to determine the strain tolerance that the material could develop when the crack lay in the localized strain field of a groove. Comparison of results with those for the basic tension series reveals that the groove had little effect on the plots of ϵ_{gnc} obtained from reduction-of-area measurements. This is surprising, because axial strain gradients should have been enhanced by the groove, and the measurements of ϵ_{ggc} plotted in the figure confirm this fact. We therefore are forced to assume that the groove does not focus strain at the crack-tip region, that is, that a given value of ϵ_{gn} did not produce as high a value of ϵ_l in this case as in tension. Confirmation of this explanation is given in Fig. 2.9, which shows the close agreement of COD values for series 5 and 6 with those for the tension series, COD being considered the best measure of relative values of ϵ_l in these tests. Similarly, the results for series 7 (single-groove bend) showed the effect of the groove to be small, and we again are forced to assume that the groove does not focus strain at the crack tip to the degree that one might expect.

Fig. 2.8. Critical strain and COD values as a function of temperature for series 3, $\epsilon_{\text{back face}}/\epsilon_{\text{cracked face}} = +0.50$, and for series 4, $\epsilon_{\text{back face}}/\epsilon_{\text{cracked face}} = +2.00$, compared with tension series (dashed lines).

Fig. 2.9. Summary plot of COD values as a function of temperature for all series.

Discussion

The gross strain concept is so simple that it is easily dismissed as being obvious, until one tries to choose locations for the strain measurements that will make them useful in the study of fracture phenomena and in the application of test data to design. Establishment of quantitative definitions for the terms used in the title of this discussion was one of the more pertinent results of the test program. We begin by first summarizing the experimental results.

Specimen cross section in the plane of the crack and crack size were constant throughout the program; hence the proximity of the crack tip to free surfaces at the net section was the same in all cases. Transverse strain gradients were obtained by testing in bending, and axial gradients were obtained by the use of grooved specimens tested in tension. A series consisted of seven specimens, which were tested at different temperatures as needed to define the transition in strain to maximum load from, nominally, 0.2 to over 2.0%. Displacements were measured at several places to define the gross strains front and back in both the net and gross sections.

As expected for this steel, the transition temperature phenomenon was exhibited by all eight series of specimen geometry and loading conditions. However, all attempts to find a common critical strain value that yielded the same temperature dependence for all eight series failed. Instead, the same pattern was obtained whether the ordinate was COD, as shown in Fig. 2.9, or gross strain in the net section at the cracked face, or at the crack-tip depth, or at midcrack depth as shown in Fig. 2.10.

The breadth of the region within the family of curves was roughly 100°, a very considerable effect of specimen and loading configuration on gross strain crack tolerance. Its lower bound was formed by data for the series loaded in bending with no backup bar, and its upper bound was that of the bend series with the backup bar welded to the cracked face. Presumably these effects may also be described as the result of strain gradients, with the highest gradient tested producing the lower bound and the reversed gradient the upper bound. The intermediate gradients generally fell logically in between these extremes.

Fig. 2.10. Summary plot of values of gross strain in the net section at midcrack depth as a function of temperature for all series.

To take a first step beyond phenomenology, we base the explanation of these strain-gradient effects on the resulting changes in constraint at the crack-tip region. This line of reasoning follows the earlier hypothesis⁹ which related ϵ_l , the local crack-tip strain, and ϵ_{lc} , its critical value for fracture, to the measured gross strain values. There was, of course, a cause-and-effect relationship between ϵ_{gn} , the gross strain in the net section, and ϵ_l through the requirements of compatibility and continuity. Beyond that, however, there was a relationship of the critical values of ϵ_{lc} to both ϵ_{gnc} and ϵ_{ggc} through their effect on constraint at the crack tip. Constraint was qualitatively defined as the degree of triaxial tension. This hypothesis seemed to explain the test results of the size-effect study as well as certain net-section effects.

To examine the application of the hypothesis that strain gradients affect GSCT through their effect on constraint, we plotted the test results on the interaction diagram, Fig. 2.11. The ordinate is intended to be the gross strain value that is the forcing function for ϵ_l , the local crack-tip strain at the point of initiation of rapid crack propagation. For series 1 to 4, it was chosen to be the value of ϵ_{gnc} (3.2 in. gage length) computed at midcrack depth. For series 5 to 7, ϵ_{gnc} at midcrack depth was a strain value obtained as described in Ref. 10. For the tension series, it was discovered that the values of ϵ_{gnc} for a 3.2-in. gage length did not fit a consistent pattern of isotherms on the interaction diagram. The values of " ϵ_{gnc} from R.A." did fit the diagram and were therefore used. The abscissa is intended to be the gross strain value that characterizes the effects of strain gradient on constraint at that point. For that, the ideal strain value would seem to be the one measured at the back face free surface where the plastic zone from the crack tip comes to the surface in plane strain deformation. Experimental strain values from several locations approximating the ideal were plotted to see which gave the most consistent picture on the interaction diagram. Values finally used were: for tension and for series 1 to 4, ϵ_{ggc} , back face, and for series 5 to 7 " $\epsilon_{back\ face}$ " obtained from the displacement in a gage length on the back face of 1.2 in. spanning the crack plane.

Data from each series should plot as a nearly straight line radiating out from the origin with increasing temperature. Disregarding net section effects and other anomalies, data for series 1 should slope upward into the second quadrant at approximately 45° . Tension should lie near the 45° line in the first quadrant, with series 4 below it and series 3 above. Series 2 should plot approximately vertically. Series 5 to 7 did not fit the diagram when gross strain in the gross section was used for the abscissa; hence an average net section strain on the back face was used as described above.

Conclusions to be drawn from the interaction diagram are as follows. The effect of strain gradients on gross strain crack tolerance can be measured by considering two gross strain values:

1. ϵ_{gnc} at midcrack depth, which seems to be the best measure of gross strain crack tolerance under strain-gradient conditions and is therefore defined as GSCT.
2. ϵ_{ggc} , back face above and below the crack, the best measure of strain-gradient effects.

From examination of Fig. 2.11, one may write a simple expression for gross strain crack tolerance (GSCT) at a given temperature under some condition of strain gradient as follows:

$$GSCT = GSCT_{tension} + f_g[\epsilon_{back\ face} - \epsilon_{back\ face, tension}] .$$

The slope f_g of the isotherms for the crack size tested is roughly +1.0. However, if one considers the broadest range of variables, the situation is probably as shown in the inset of Fig. 2.11. At the very low temperatures at which plastic strains are confined to the vicinity of the crack tip, constraint should be constant at its maximum value, irrespective of back face strain. At high temperatures, where failure is by tensile instability rather than by crack propagation, back face strain again should be unimportant. Hence f_g is nearly zero at temperatures representing the bounds of the transition region and is approximately +1.0 in the center of the region.

To explain the upward slope of the isotherms with increasing back face *tensile* strain, we rely on slip line field theory to provide a picture of flow emanating from the crack-tip region. It can occur by wedge

actions in both of the principal transverse directions. Measurement of thickness reduction revealed that this did in fact take place. The effect of a favorable strain gradient is to facilitate the establishment of the wedge-action flow pattern at low values of ϵ_{gn} ; that is, by relieving constraint, it causes ϵ_{lc} to rise to a high value before ϵ_l becomes large. When bending produces a neutral axis, a region of near zero strain below the crack tip, the necessary wedge action cannot develop.

The strong interaction of ϵ_{gnc} at midcrack depth, the forcing function for crack propagation, and $\epsilon_{back\ face}$, the agent for relief of constraint, has implications for the workability of energy concepts. It suggests that a concept of total energy absorbed within the volume of metal defined by the specimen cross section and a gage length of comparable dimensions is not likely to be completely sufficient for predicting the results for one strain gradient from another, if one of them is the bending case where some strain is compressive and the other is not. Compressive strain on the back face of a beam contributes to the total energy just as tensile strain at the back face does in a tensile test, but our work shows that they have opposite effects on gross strain crack tolerance.

Elastic-Plastic Behavior of a Longitudinal Semielliptical
Crack in a Thick Pressure Vessel¹¹

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This report summarizes the most current work performed on the development of finite-element methods for three-dimensional elastic-plastic stress analysis in fracture mechanics. Previous work^{12,13} was concerned

¹¹Work being performed under UCCND Subcontract No. 3153 between Union Carbide Corporation and Brown University.

¹²N. Levy and P. V. Marcal, *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics. Phase I. Simple Flawed Specimens*, HSST D-TR-12 (December 1970).

¹³N. Levy and P. V. Marcal, *Three-Dimensional Elastic-Plastic Stress and Strain Analysis for Fracture Mechanics. Phase II. Improved Modeling*, HSST-TR-17 (November 1971).

with developing a large-scale computer program for elastic-plastic analysis and also with exploring the use of the special crack-tip elements¹⁴ in three dimensions. From previous results in Ref. 13, it was still obvious that a three-dimensional elastic-plastic analysis would be expensive, because of the requirements of mesh refinement near the crack tip. In order to avoid this expenditure, we have returned to our earlier development,¹² which gave improved modeling by coalescing nodes of isoparametric elements at the crack tip.¹⁵ It was decided to generate three-dimensional meshes with the special crack-tip elements around the semi-elliptical crack in a tensile specimen by a warping process, the same procedure used for generating isoparametric elements. The tensile specimens were then mapped to obtain thick-walled cylinders.

This report presents results for the elastic stress intensity factors obtained with the special crack-tip elements and also traces the elastic-plastic behavior of a flawed thick-walled pressure vessel with the same dimensions of the vessel to be tested under the HSST program.

Mesh generation

The mesh was generated by means of a three-dimensional mesh generator MARCMESH3D, which first creates blocks of elements and then merges these elements. Figure 2.12 gives a perspective plot of the blocks used to create the special tip elements around the elliptical crack. A quarter of the elliptical crack is shown on the top surface of the block with the crack. Figure 2.13 gives a plot of the top layer of elements with the crack. It shows the wedge-shaped elements obtained around the crack tip (e.g., at the common boundary between elements 3 and 79). This mesh with three elements placed in each direction normal to the crack will be called the 3×3 mesh. Figure 2.14 shows a layer of elements at $x = 0$, the plane of symmetry about the minor axis. The elements belong to the 3×3 mesh

¹⁴N. Levy et al., "Small Scale Yielding Near a Crack in Plane Strain: A Finite Element Analysis," *Int. J. Fract. Mech.* 7(2), 143-56 (1971).

¹⁵J. R. Rice and D. M. Tracey, "Computational Fracture Mechanics," *Proc. Symposium on Numerical and Computer Methods in Structural Mechanics, Urbana, Illinois, September 1971.*

Fig. 2.12. Plot of blocks for elliptical-crack problem.

Fig. 2.13. Plot of elements for 3×3 mesh with $y = 0$.

Fig. 2.14. Plot of elements for 3×3 mesh with $x = 0$.

used for the analysis of the thick-walled cylinder with a longitudinal semielliptical crack.

Elastic analysis

A semielliptical crack of 7 in. major and 4 in. minor diameter was placed on the surface of a tensile specimen as shown in Fig. 2.15. Because of symmetry, only one quadrant of the 6 × 18 × 18 in. tensile specimen was analyzed. Two idealizations were used: a 2 × 2 mesh pattern with 72 elements and 126 nodes; and a 3 × 3 mesh with 216 elements and 322 nodes.

Fig. 2.15. Geometry of cylinder and crack analyzed.

The following formulas were used to obtain the normalized stress intensity factor on the basis of displacements at the crack tip and the actual stress in the element adjacent to the crack tip:

$$K_{\text{disp}} = \delta(2\pi/r)^{1/2} [2G/4(1 - \nu)]/\sigma_{\infty} ,$$

$$K_{\text{st}} = \sigma(2\pi r)^{1/2}/\sigma_{\infty} ,$$

where

δ = normal displacement of a point in the plane of the crack;

r = distance from the crack tip;

σ = normal stress of a point in the plane of the crack;

σ_∞ = nominal stress, in the case of the cylinder with internal pressure $\sigma_\infty = pd/2t$.

The analyses were repeated with the two meshes after the coordinates were mapped so as to form a quadrant of a cylinder as shown in Fig. 2.16. Recall that Fig. 2.14 gave a side view ($x = 0$) of the element disposition around the crack tip. Symmetry conditions were placed on the cylinder so as to require the analysis of only one quadrant. The planes normal to

Fig. 2.16. View of cylinder sector showing element boundaries at surface for 3×3 block division.

the longitudinal direction were fixed relative to each other so that plane strain conditions would be approximated in the x direction. An internal pressure was applied to the inside surface, and corresponding loads were also applied on nodes on the crack surface. The stress intensity factors obtained by the elastic analyses are summarized in Table 2.5. The stress intensity factors based on displacements were found from the displacements at the first free node away from the crack tip ($r = 0.5$ in. for 3×3 mesh), and the factors based on stresses were calculated from the stress value at the centroid of the element adjacent to the crack tip ($r = 0.5$ in. for 3×3 mesh).

Table 2.5. Normalized stress intensity factor

Specimen	2 × 2 Mesh			3 × 3 Mesh		
	Displacement	Stress	Mesh	Displacement	Stress	Mean
Tensile	2.27	3.55	2.9	2.47	2.97	2.7
Cylinder	3.08	5.00	4.0	3.60	4.06	3.8

The results from the 2×2 and 3×3 mesh show a convergence of the stress and displacement based normalized stress intensity factors, so that it is reasonable to estimate a mean value of $2.7 \text{ in.}^{1/2}$ and $3.8 \text{ in.}^{1/2}$ for the tensile and cylindrical specimen respectively. The mean value of $2.7 \text{ in.}^{1/2}$ may be compared with the values of $1.8 \text{ in.}^{1/2}$ from Ref. 13 and $2.25 \text{ in.}^{1/2}$ from Ref. 16. This ability to obtain a normalized stress intensity factor with such a coarse mesh is encouraging and paves the way for exploration of the effect of embedding cracks in other more-complex structures.

Elastic-plastic analysis

As an example problem an elastic-plastic analysis of a thick cylinder under internal pressure was then carried out. A 2×2 mesh with assumed

¹⁶G. R. Irwin, "Crack-Extension Force for a Part-Through Crack in a Plate," *J. Appl. Mech., Trans. ASME* 28(4), 651-54 (1962).

values of Young's modulus = 30×10^6 psi, Poisson's ratio = 0.3, and a yield stress = 20,000 psi was used. An elastic-perfectly plastic von Mises material was assumed.

The region of highest equivalent stress was found to be that around the crack tip and on the inner surface of the cylinder. Thus the region of highest stress intensity factor did not coincide with that of highest equivalent stress. The model was found to yield first at a pressure of 4200 psi and reached its limit pressure at around 9200 psi. This may be compared with a limit pressure of 8800 psi for an unflawed pressure vessel.¹⁷ The loads were scaled up to cause yielding at the most highly stressed element. Subsequently, loads were applied in increments of 0.2 times the load to cause first yield until the limit load was reached. It should be noted that the first yield pressure is dependent on the size of the mesh and that this was not the objective of the calculation. The objective was to get a broad idea of what happens to the stress and strain field in the specimen. Figure 2.17 shows the variation of the maximum equivalent plastic strain with respect to pressure, and Fig. 2.18 shows that the mean stress increases almost linearly with increase in pressure. It is interesting to note that the mean stress is almost double the yield stress at the limit load. The plastic yield front is followed in a series of diagrams which show its development with increase in load. It is interesting to note the high equivalent stress on the $x = 0$ plane in the region away from the crack and the yield caused by it on the fourth increment (Fig. 2.19). This is caused by the Lamé effect. The crack relieves this equivalent stress in the crack line, where gross yielding does not occur even at the limit load. The plane at $x = 9$ in., however, was completely yielded after the fifth increment of load. Figure 2.20 traces the development of the plastic region throughout the structure.

¹⁷P. V. Marcal, "On the Elastic-Plastic Thick Cylinder with Internal Pressure in the Open and Closed-End Condition," *Int. J. Mech. Sci.* 7, 841-45 (1965).

Fig. 2.17. Variation of equivalent plastic strain with load at crack tip for 2×2 mesh element No. 45.

Fig. 2.18. Variation of mean normal stress with pressure at crack tip for 2×2 mesh element No. 45.

Fig. 2.19. Plastic yield fronts.

Conclusions and recommendations for future work

A method for generating the mesh with special crack elements in which the nodes coalesce at the tip has been demonstrated. The resulting model has been shown to be very effective in obtaining stress intensity factors. A coarse mesh with 216 nodes gave results which were similar to those obtained in Ref. 13 with 1080 elements. The effect of embedding a semielliptical crack in a thick-walled cylinder is to produce a stress intensity factor approximately 40% higher than that in a tensile specimen.

The elastic-plastic analysis of the 2×2 mesh produced results as expected in a cylinder with a crack. The yielded zones on the $x = 0$ plane of symmetry appear like those usually found in plane strain specimens. The existence of the crack did not significantly lower the limit load in

Fig. 2.20. Plastic yield fronts.

the cylinder. This is similar to the limit load found for tensile specimens in Ref. 13.

Blocks containing cracks can now be generated with ease, and these blocks can be embedded in a variety of complex structures. The possibility of using a relatively coarse mesh together with a few crack-tip elements in an analysis paves the way for parametric studies in three-dimensional elastic and elastic-plastic fracture mechanics. Further analyses should be made of cracks in complex structures.

Experimental Verification of Lower-Bound K_{Ic} Values
Utilizing the Equivalent-Energy Concept¹⁸

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Witt's¹⁹ equivalent-energy method results in fracture toughness K_{Icd} values which can be shown to be lower-bound K_{Ic} values for a given specimen thickness. The calculation of the lower-bound K_{Ic} values is very sensitive to the shape of the load-displacement curve. Since the ASTM Recommended Practice for Plane-Strain Fracture Toughness Testing²⁰ permits use of a constant gage length and a constant elevation beyond the test specimen's surface for displacement measurement, the question was raised as to what effects result from a varying geometric proportionality with increasing specimen thickness.

Effect of various relative displacement gage lengths on K_{Icd} measurements

A 6T CT specimen was adapted to accommodate four LVDT gages. The four gage lengths were chosen to represent the different geometric

¹⁸Work performed under UCCND Subcontract No. 3196 between Oak Ridge National Laboratory and Westinghouse Electric Corporation.

¹⁹F. J. Witt and T. R. Mager, *A Procedure for Determining Bounding Values on Fracture Toughness at Any Temperature*, ORNL-TM-3894 (in publication).

²⁰"Proposed Method of Test for Plane-Strain Fracture Toughness of Metallic Materials," in *ASTM Standards (1969)*, Part 31, *Physical and Mechanical Testing of Metals; Nondestructive Tests*, pp. 1099-1114, American Society for Testing and Materials, Philadelphia, Pa., 1969.

proportionalities in use for the measurement of 1T, 2T, 4T, and 6T CT specimens. The current Westinghouse testing procedure is to use a 2.25-in. gage length and a constant 0.1875-in. elevation above the specimen surface for displacement measurements of all specimen sizes.

The 6T CT specimen was precracked at room temperature and tested at 200°F. The test temperature of 200°F was chosen to guarantee plasticity in the specimen. The gage lengths and elevation heights from the specimen surface were chosen to reflect the geometric proportionality of the specimen being simulated. The gage lengths and mounting-block heights for the various simulated sizes are given in Table 2.6.

Table 2.6. Displacement gage lengths and mounting block heights for the various simulated specimen sizes

Simulated specimen size	Gage length used on the 6T (in.)	Mounting block height used on the 6T (in.)
1T	13.500	1.1250
2T	6.750	0.5625
4T	3.375	0.2813
6T	2.250	0.1875

Four separate load-displacement curves were recorded on x - y recorders while the specimen was loaded at 200°F. A single load signal was fed into all four x - y recorders. Using the equivalent-energy method as set forth by Witt,²¹ K_{Icd} values were calculated from the four load-displacement records. The K_{Icd} values calculated for simulated 1T, 2T, 4T, and 6T CT specimens are given in Table 2.7. Included with each calculation is an error range which was estimated from the uncertainty associated with locating the initial incidence of maximum load. There was no distinguishable

²¹F. J. Witt, *Equivalent Energy Procedure for Predicting Gross Plastic Fracture*, ORNL-TM-3172 (1970).

Table 2.7. Calculations for equivalent-energy analysis

Simulated specimen size	Deflection at maximum load (in.)	K_{Icd} (ksi $\sqrt{\text{in.}}$)	Limits of error (ksi $\sqrt{\text{in.}}$)
1T	0.300	355	± 6
2T	0.300	368	± 22
4T	0.280	341	± 17
6T	0.275	354	± 23

variation or trend which could be attributed to the different gage lengths and gage positions employed.

It was therefore concluded that calculations of K_{Icd} would not exhibit measurable variations resulting from differences in displacement measurement geometry.

Lower-bound K_{IC} testing

The objective of this work was to experimentally verify the lower-bound K_{IC} concept as proposed by Witt.²¹ Two grades of materials were evaluated, the first of which was A533, grade B, class 1 steel (HSST plate 03). Eight 3T CT specimens (three oriented in the RW direction and five oriented in the WR direction) and sixteen 0.394T CT specimens were fabricated from the A533, grade B, class 1 steel. (The sixteen 0.394T CT specimens were fabricated by ORNL.)

The second material studied was a portion of an NiCrMoV rotor forging. Rotor-forging-grade steel was tested because Begley²² had previously demonstrated that a valid K_{IC} upper shelf existed for this particular heat of steel. Ten 1T CT, six 2T CT, and three 4T CT specimens were fabricated from the steel.

²²J. A. Begley and P. R. Toolin, *Fracture Toughness and Fatigue Crack Growth Rate Properties of a NiCrMoV Steel Sensitive to Temper Embrittlement*, Westinghouse Research Laboratory Scientific Paper 71-1E7-BFSGT-P1 (Apr. 23, 1971).

Prior to testing, the specimens were instrumented with a front face clip gage, and the load-time behavior was recorded. After the precracking operation the specimens were tested at various preselected temperatures. The temperature of the specimens prior to the testing was monitored, using copper-constantan thermocouples, until the surface temperature stabilized to $\pm 2^\circ\text{F}$.

Experimental results

The load-deflection curves were analyzed using the method outlined by Witt in Refs. 19 and 21.

Specimens fabricated from A533, grade B, class 1 steel were tested at temperatures from -50 to 550°F . The crack lengths and various fracture toughness test results obtained from the specimens fabricated from the A533, grade B, class 1 steel are shown in Tables 2.8 and 2.9.

The 3T CT specimens from which the data in Table 2.9 were obtained were machined from a broken half of a 9T CT specimen previously tested at 550°F . Each specimen had one face only $1\frac{1}{2}$ in. interior to a quenched and tempered surface for which the toughness is much higher at lower temperatures than for center material. The location of the specimen, together with the possibility of prior plastic strain, would tend to increase the apparent lower-bound toughness at lower temperatures. Thus the results around 100°F are rather suspect and should probably not be considered as reliable data.

Specimens fabricated from the NiCrMoV rotor forging material were tested at eight temperatures in the range from 0 to 550°F . All the specimens were oriented in the tangential direction. The test results are summarized in Table 2.10 and in Fig. 2.21. Also shown in Fig. 2.21 is the curve reported by Begley²² for this heat of steel. All the data points fall close to the valid K_{IC} curve reported by Begley.²²

The analysis of the rotor forging was influenced by the occurrence of "pop-in" during testing. Begley²² analyzed the load-deflection curves according to the ASTM recommended practice,²⁰ but the results reported here disregard the occurrence of "pop-in." The data will be further analyzed to evaluate the influence of the "pop-in" and/or slow ductile tearing prior to maximum load on the apparent fracture toughness K_{Icd} .

Table 2.8. K_{Iod} fracture toughness results determined from testing 0.39WT CT specimens (RW orientation) using various notch preparations

Specimen No.	Specimen Size (in.)	Notch preparation	Test temp. (°F)	Crack length (in.)	Some fracture toughness parameters ^a (ksi√in.)				PQ (lb)	K_{Iod} (ksi√in.)	Comments
					KD	KQ	KU	KP			
506	0.394	Fatigue cracked	-50	0.360	35.3	48.1	63.0	16.0	1000	76.6	
505	0.395	Fatigue cracked	-50	0.373	30.0	49.0	72.0	17.0	1000	92.0	
512	0.394	Fatigue cracked	0	0.520	35.4	42.0	57.2		700	74.2	
511	0.394	Fatigue cracked	0	0.411	283.	42.0	66.0	19.1	700	101.0	
530	0.394	Fatigue cracked	+50	0.408	29.3	42.3	73.2	19.0	675	125.0	
529	0.394	Fatigue cracked	+50	0.360	32.0	41.4	80.4	16.0	1000	135.0	
536	0.394	Fatigue cracked	+200	0.423	30.0	41.0	72.0	20.1	750	149.0	
537	0.394	Fatigue cracked	+200	0.375	26.1	30.0	79.0	17.0	875	159.0	
503	0.393	Electron beam	-50	0.380	20.0	31.5	57.0		600	35.0	Precracked 40 to 60 mils by ORNL
504	0.393	See comments	-50	0.340	37.0	52.3	70.0		1280	83.2	No precrack or electron-beam weld
511	0.392	Electron beam	0	0.403	20.1	44.0	55.0		500	46.0	Precracked 40 to 60 mils by ORNL
510	0.393	Electron beam	0	0.345	29.0	44.1	79.0		1000	118.0	No precrack
527	0.393	Electron beam	+50	0.340	37.0	48.0	76.0		500	61.0	Precracked 40 to 60 mils by ORNL
528	0.393	Electron beam	+50	0.380	34.3	46.0	76.0		750	167.0	Precracked 40 to 60 mils by ORNL
534	0.393	Electron beam	+200	0.381	34.2	43.0	74.0		1000	215.0	Precracked 40 to 60 mils by ORNL
535	0.393	Electron beam	+200	0.385	24.0	41.0	75.0		500	227.0	Precracked 40 to 60 mils by ORNL

^aKD = fracture toughness at deviation load displacement, KQ = fracture toughness at secant offset load, KU = fracture toughness at ultimate load, and KP = fracture toughness during pre-fatigue precracking.

Table 2.9. K_{Iod} fracture toughness results determined from testing 3T CT specimens oriented in both the RW and WR directions (HSST plate 03T)

Specimen No.	Orientation	Test temp. (°F)	Crack length (in.)	Some fracture toughness parameters ^a (ksi√in.)				PQ (lb)	K_{Iod} (ksi√in.)
				KD	KQ	KU	KF		
3C9-3	RW	+100	3.133	67.1	110.0	172.0	16.0	40,000	289.1 ^b
3C9-1	RW	+200	3.340	34.4	70.5	172.4	18.0	20,000	360.0 ^c
3C9-2	RW	+500	3.156	31.1	93.3	165.0	16.2	20,000	293.0
3C9-5	WR	+100	3.151	65.3	114.0	175.0	16.3	40,000	295.1 ^b
3C10-4	WR	+100	3.126	76.0	109.1	168.0	16.0	50,000	279.0 ^b
3C10-1	WR	+130	3.126	61.3	103.1	168.0	16.0	40,000	280.0
3C10-2	WR	+200	3.113	55.3	109.3	166.1	16.0	35,000	282.2
3C10-3	WR	+550	3.146	70.4	103.0	156.3	16.2	40,000	240.1

^aParameters defined in Table 2.8.

^bSee text for discussion of these data.

^cClip gage failed.

Table 2.10. K_{Icd} fracture toughness results determined from testing 1T, 2T, and 4T compact-tension specimens of NiCrMoV forging steel

Specimen No.	Specimen size	Test temp. (°F)	Crack length (in.)	Some fracture toughness parameters ^a (ksi√in.)				PQ (lb)	K_{Icd} (ksi√in.)	Comments
				KD	KQ	KU	KF			
NCM-6	1T	0	1.060	53.4	79.0	79.0	16.3	6,000	82.0	Max. load
NCM-10	1T	0	1.066	82.5	99.4	99.4	16.5	10,000	104.2	Max. load
NCM-9	1T	+100	1.073	77.3	114.5	118.0	17.0	8,000	125.0	Max. load
NCM-7	1T	+100	1.051	58.5	79.0	128.1	16.1	7,000	138.0	Max. load
NCM-8	1T	+150	1.075	53.2	88.2	134.2	17.0	5,000	149.0	Max. load
NCM-4	1T	+200	1.066	66.0	100.5	145.1	16.5	8,000	169.0	Pop-in
NCM-1	1T	+250	1.070	90.1	112.0	151.4	17.0	10,000	171.0	Pop-in
NCM-5	1T	+250	1.053	69.0	106.0	141.3	16.1	8,000	178.4	Pop-in
NCM-3	1T	+300	1.056	71.0	108.0	140.2	16.2	8,000	168.0	Pop-in
NCM-2	1T	+500	1.065	65.0	96.0	132.0	16.4	8,000	142.4	Pop-in
1196-84	2T	0	2.053	76.0	76.0	76.0	16.0	15,000	76.2	Valid
1196-85	2T	+100	2.068	77.0	104.1	111.3	16.0	25,000	91.4	Max. load
1196-88	2T	+100	2.066	68.1	70.0	129.4	16.0	20,000	107.0	Pop-in
1196-86	2T	+200	2.060	81.0	127.2	175.1	16.0	25,000	179.0	Pop-in
1196-83	2T	+250	2.071	108.0	150.0	176.4	17.3	30,000	198.0	Max. load
1196-87	2T	+550	2.055	76.0	116.3	152.0	16.0	25,000	155.4	Pop-in
1196-103	4T	+200	4.125	121.0	181.5	203.0	16.0	120,000	233.3	Max. load
1196-102	4T	+250	4.128	130.1	178.2	203.2	16.0	120,000	202.0	Pop-in
1196-104	4T	+250	4.096	125.0	181.3	211.0	15.5	120,000	214.3	Pop-in

^aSee Table 2.8 for parameter definitions.

Fig. 2.21. Variation of K_{Icd} as a function of temperature for tangentially oriented compact-tension specimens of NiCrMoV rotor forging steel.

STATIC AND DYNAMIC FRACTURE TOUGHNESS TESTS ON PRECRACKED CHARPY SPECIMENS

W. J. Stelzman

The study to measure the potential of the Charpy-type specimen for determining the fracture toughness parameter is continuing. Results obtained to date are presented and compared where possible with similar results from Charpy-thickness compact-tension specimens.

Precracking was done by either the fatigue method or the electron-beam weld method. The electron-beam weld method, proposed by Canonico,^{2,3}

^{2,3}D. A. Canonico, "Technique for Generating Sharp Cracks in Low-Alloy High Strength Steels," *HSST Program Semiannu. Progr. Rep.* Feb. 28, 1971, ORNL-4681, pp. 60-64.

utilizes the electron-beam weld and hydrogen introduced into the weld zone electrolytically to generate cracks, after which the hydrogen is baked out at 400°F for 2 hr. All the material used in the study came from plate section 01K. The specimens were designated as "select," because the method of preparation and testing depended upon the fate of its neighbor, or "general," because the same specimen relationship was not so closely controlled. The fracture toughness parameters K_{Icd} and K_{Idd} were determined by the equivalent-energy method of calculation proposed by Witt²⁴ and ASTM Standard E 399-70T.²⁵

The tensile results from 0.178-in.-gage-diam specimens from "select" locations are shown in Fig. 2.22 at several temperatures. These data agree

²⁴F. J. Witt, "The Equivalent Energy Method for Calculating Elastic-Plastic Fracture," Paper 12, HSST Program Fifth Annual Information Meeting., Mar. 25-26, 1971.

²⁵1970 Annual Book of ASTM Standards. Part 31. Metals - Physical, Mechanical, Nondestructive, and Corrosion Tests, Metallurgy, Fatigue, Effect of Temperature, American Society for Testing and Materials, E 399-70T, pp. 911-27, July 1970.

Fig. 2.22. Temperature dependence of tensile properties of 1/4T, X-oriented miniature tensile specimens from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

with previously published data²⁶ from plate section 01K at the 1/4T level and R orientation.

Before we could start the program, it was necessary to determine the dependence of the fracture toughness parameter on the stress applied to the specimen at the onset of the fatigue cycle. This was necessary to determine the highest stress level which could be applied consistent with a reasonable fatigue cycle time without affecting the fracture toughness parameter. Figure 2.23 shows the effect of the outer fiber stress on the fracture toughness from slow-bend testing K_{Icd} at four temperature levels. Taking into account the scatter of the data at each temperature level, no significant change in fracture toughness is apparent. The fatigue cycle times ranged from 8 min to 8 hr.

Figure 2.24 shows these same data plotted to show the sensitivity of K_{Icd} to the depth of precracking at the various stress levels. Again the scatter of the data obscures any significant change in K_{Icd} over the temperature range or precracking range studied.

Table 2.11 outlines the portion of the program completed. The first step was to determine the effect of temperature on the fracture toughness parameters K_{Icd} and K_{Idd} when notching and precracking the Charpy bar to the standard 0.080-in. depth and testing in impact (dynamic) and slow bend (static). These results are shown in Figs. 2.25 and 2.26. The second step was to determine the effect of temperature on K_{Icd} and K_{Idd} when notching to two depths, 0.040 and 0.080 in., and precracking by fatiguing to a total depth of 0.198 in. or middepth of the Charpy bar. This would give a crack length-to-specimen thickness ratio (a/W) equal to 0.5, similar to the compact-tension specimen requirements. These specimens were also tested in impact and slow bend, and the results are shown in Figs. 2.27 and 2.28.

The data in Figs. 2.25 and 2.27 show that the fracture toughness values from impact or dynamic testing, K_{Idd} , are not significantly affected by the precrack depth between 0.080 and 0.200 in., the 0.040- or 0.080-in. depth of notch when precracked to the 0.200-in. depth, or the

²⁶R. G. Berggren, "Material Characterization and Variability," *HSST Program Semiannu. Progr. Rep. Feb. 28, 1969*, ORNL-4463, pp. 20-29.

Fig. 2.23. Dependence of K_{Iod} fracture toughness on the outer fiber stress at the start of fatigue cracking 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

Fig. 2.24. Dependence of K_{Iod} fracture toughness on fatigue-crack depth in 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

Table 2.11. Static and dynamic fracture toughness testing on precracked Charpy specimens of HSST plate 01 (ASTM A533, grade B, class 1)

Crack starter	Nominal denth of fatigue crack (in.)	Nominal depth of electron-beam weld crack (in.)	Type of test
0.080-in.-deep broached notch	None	None	Impact and slow bend
0.080-in.-deep broached notch	0.200	None	Impact and slow bend
0.040-in.-deep broached notch	0.080	None	Impact and slow bend
0.040-in.-deep broached notch	0.200	None	Impact and slow bend
0.040-in.-deep broached notch	None	0.080	Impact
Electron-beam weld	None	0.080	Slow bend

DYNAMIC TESTING

0.040-in.-DEEP BROACHED NOTCH SPECIMEN
 ■ FATIGUE CRACKED TO 0.077-0.112 in. DEEP
 ■ FATIGUE CRACKED TO 0.092-0.119 in. DEEP
 ▲ ELECTRON BEAM WELD CRACKED TO 0.083-0.093 in. DEEP

0.080-in.-DEEP BROACHED NOTCH SPECIMENS
 ○ BROACHED NOTCH ONLY

OPEN POINTS INDICATE "SELECT" MATERIAL
 CLOSED POINTS INDICATE "GENERAL" MATERIAL

Fig. 2.25. Temperature dependence of K_{Icd} fracture toughness of notched, fatigue-cracked, and electron-beam-weld cracked 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

SLOW BEND TESTING

0.040-in.-DEEP BROACHED NOTCH SPECIMENS
 ■ FATIGUE CRACKED TO 0.081-0.085 in. DEEP
 ■ FATIGUE CRACKED TO 0.88 in. DEEP

0.080-in.-DEEP BROACHED NOTCH SPECIMENS
 ○ BROACHED NOTCH ONLY

ELECTRON BEAM WELD
 ▲ CRACKED TO 0.088-0.102 in. DEEP

OPEN POINTS INDICATE "SELECT" MATERIAL
 CLOSED POINTS INDICATE "GENERAL" MATERIAL

Fig. 2.26. Temperature dependence of K_{Icd} fracture toughness of notched, fatigue-cracked, and electron-beam-weld cracked 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

DYNAMIC TESTING

0.040-in.-DEEP BROACHED NOTCH SPECIMENS

- FATIGUE CRACKED TO 0.198-0.213 in. DEEP
- FATIGUE CRACKED TO 0.197-0.217 in. DEEP

0.080-in.-DEEP BROACHED NOTCH SPECIMENS

- FATIGUE CRACKED TO 0.191-0.214 in. DEEP

OPEN POINTS INDICATE "SELECT" MATERIAL

CLOSED POINTS INDICATE "GENERAL" MATERIAL

Fig. 2.27. Temperature dependence of K_{Idd} fracture toughness of notched and fatigue-cracked 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

SLOW BEND TESTING

0.040-in.-DEEP BROACHED NOTCH SPECIMENS

- FATIGUE CRACKED TO 0.190-0.207 in. DEEP
- FATIGUE CRACKED TO 0.202-0.266 in. DEEP

0.080-in.-DEEP BROACHED NOTCH SPECIMENS

- FATIGUE CRACKED TO 0.199-0.218 in. DEEP
- FATIGUE CRACKED TO 0.144-0.243 in. DEEP

OPEN POINTS INDICATE "SELECT" MATERIAL

CLOSED POINTS INDICATE "GENERAL" MATERIAL

Fig. 2.28. Temperature dependence of K_{Idd} fracture toughness of notched and fatigue-cracked 1/4T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HSST plate 01).

method of precracking to the 0.060-in. depth. Also, the "notched only" specimens provide higher fracture toughness values, signifying that the fracture toughness transition for the "notched only" specimens occurs at a lower temperature than for the precracked specimens. These data agree quite well with data obtained by Shabbits²⁷ from larger compact-tension specimens.

The data shown in Figs. 2.23, 2.24, 2.26, and 2.28 show that the fracture toughness values from the slow bend testing, K_{Icd} , are not significantly affected by the outer fiber stress, temperature, or precrack range of this study, nor are they sensitive to the 0.040- or 0.080-in. depth of notching prior to the precracking. The "notched only" specimens also provide higher fracture toughness values than the precracked specimens, and indications are that the slow-bend test will provide fracture toughness transitions at lower temperatures than comparable dynamic tests. Also, the data appear to indicate that the electron-beam weld precracking may provide lower fracture toughness values than fatigue precracking when followed by slow-bend testing. For the present, the K_{Icd} values appear to be somewhat higher than those obtained by Shabbits²⁸ from larger compact-tension specimens from plate 02 material. However, fracture toughness values from the Charpy-thickness compact-tension specimens tested by Mager²⁹ from "select" locations in the study plate section 01K, shown in Fig. 2.29 together with the scatter band of the data shown in Fig. 2.28, agree quite well. This raises the question of whether there is a difference in fracture toughness between plates 01 and 02. Steps are being taken to resolve this problem. Figure 2.30 shows the impact energy variation with test temperature for the 0.080-in.-depth electron-beam weld cracked specimens.

²⁷W. O. Shabbits, *Dynamic Fracture Toughness Properties of Heavy Section A 533 Grade B Class 1 Steel Plate*, WCAP-7623 (December 1970).

²⁸W. O. Shabbits, W. H. Pryle, and E. T. Wessel, *Heavy-Section Fracture Toughness Properties of A 533 Grade B Class 1 Steel Plate and Submerged Arc Weldment*, WCAP-7414 (December 1969).

²⁹T. R. Mager, Westinghouse Electric Corporation, personal communication to F. J. Witt.

Fig. 2.29. Comparison of fatigue-precracked Charpy-size compact-tension specimens and fatigue-precracked Charpy bars.

Fig. 2.30. Results of impact testing of electron-beam-weld precracked 1/3T, RW-oriented Charpy bars from 12-in.-thick A533, grade B, class 1 steel (HESST plate 01).

Further testing and analysis of these results are necessary and are in progress. When the study has been completed, a complete and comprehensive report will be issued.

FATIGUE-CRACK-GROWTH STUDY OF NUCLEAR PRESSURE VESSEL
MATERIALS IN SIMULATED REACTOR ENVIRONMENT³⁰

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Some of our experiments concerning the influence of high-temperature aqueous environment on the character of fatigue crack growth were reported³¹ at the 5th HSST Information Meeting. In the current study emphasis has been placed on (1) critical examination of the experimentally observed fact that crack growth is frequently interrupted when the frequency of strain cycling is lowered below a certain level, and (2) analysis of the fatigue data obtained in various environmental and mechanical conditions by means of the da/dN vs $(\Delta K)^n$ fracture mechanics correlation.

The materials under investigation are ASTM A302, grade B (the metallurgical data are listed in Ref. 31) and A533, grade B. Specimens of A533, grade B were obtained from a Japanese domestic heat (the metallurgical data are shown in Tables 2.12 and 2.13).

³⁰This work was sponsored and performed by Japan Atomic Energy Research Institute and is reported under an informal cooperative agreement with the HSST Program.

³¹T. Kondo et al., "Corrosion Fatigue of ASTM A 302 Grade B Steel in High Temperature Water, the Simulated Nuclear Reactor Environment," Paper 2, 5th Annual Information Meeting of the HSST Program, March 1971.

Table 2.12. Chemical composition of the Japanese domestic A533, grade B, class 1 material

Sampling	Chemical composition (wt %)									
	C ^a	Si	Mn	P	S	Cu	Ni	Cr	Mo	Al
Ladle	0.18	0.25	1.34	0.008	0.004	0.05	0.60	0.10	0.51	0.036
Check top	0.18	0.26	1.37	0.008	0.005		0.61		0.53	
Check bottom	0.19	0.26	1.38	0.008	0.006		0.61		0.53	

^aThe original ingot was prepared for a 150-mm-thick plate. The carbon content is therefore slightly higher than the normal level for 35-mm-thick plates.

Table 2.13. Tensile properties of the Japanese domestic A533, grade B, class 1 plate,^a RW orientation

Test temperature (°C)	Plate position	Yield strength, 0.2% offset (psi)	Ultimate strength (psi)	Total elongation (%)
		$\times 10^3$	$\times 10^3$	
Room	Top	51.8	96.7	27
Room	Bottom	51.9	96.7	28
350	Top	57.0	92.6	36

^aPlate dimensions: 40 × 16 × 1.4 in.; heat treatment: austenitize at 920°C for 60 min, air cool, temper at 700°C for 120 min, air cool, temper at 710°C for 120 min, air cool.

Effect of Some Experimental Parameters on Crack-Growth Incubation in Corrosion Environment³²

At the 5th HSST Information Meeting,³¹ it was hypothesized that formation of macroscopic corrosion pits at the tips of growing cracks was responsible for the blunting of cracks. Work has been continued on this hypothesis to determine which of several conceivable experimental parameters affect the trend of the growth incubation of fatigue cracks in an aqueous environment.

Low-frequency fatigue tests were carried out under a variety of experimental conditions to examine the hypothesis, with the following factors selected as variables:

1. Crack geometry. Open, 45° V-notched specimens did not show incubation until the fatigue cracks grew sufficiently deep to favor the formation of oxygen-concentration cells within the cracks, while specimens with 6-mm-deep precracks readily showed incubation trend.

2. Galvanic (two-metal) effect. Existence of stainless steel weld overlay was not an essential requisite for incubation.

³²T. Kondo et al., "Fatigue of Low Alloy Steels in High Temperature Aqueous Environment," *Proc. International Conference on Mechanical Behavior of Materials, Kyoto, Japan, vol. III (1972).*

3. Waveform effect. In the displacement-controlled, tension-to-compression type tests, a sinusoidal waveform provided a condition causing incubation. On the other hand, a rectangular waveform did not cause incubation in the tests attempted down to 0.5 cpm. Figure 2.31 illustrates a comparison of the results for 1- and 0.5-cpm tests made in 200°C water.

The scanning electron microscope examination of the fatigue-fractured surfaces indicated that the occurrence or absence of the growth incubation in the various types of tests strictly coincided with the existence or absence of the corrosion pits in the fractured surfaces. The distorted patterns of the striations in the fracture surfaces are thought to be evidence of the interaction of the growing crack fronts with the pits.

From these observations it is concluded that the conditions essential for the occurrence of growth incubation are as follows:

1. Formation of narrow crevices with fatigue cracks to provide an environmental condition that is favorable for pitting.
2. Continual change in the stress condition at the crack tips, which keeps the crack fronts active by continually developing the local plastic flow. The ever-existing difficulty in establishing a stable passivity under such a mechanochemical condition makes pitting corrosion attack occur preferentially at the crack fronts. The formation of the pits at the crack tips may geometrically blunt them as well as remove considerably

Fig. 2.31. Effect of loading frequency on crack growth under displacement-controlled rectangular strain wave condition. Material: A302, grade B with precracked weld overlay.

large parts of severely strained metal by continuous bursts of dissolution.

The corrosion characteristics of A302 B and A533 B steel are considered to be essentially equivalent, so that the conclusions drawn are applicable to both materials.

Crack Propagation Rate as a Function of Stress Intensity Factor Range, ΔK_I

The fatigue test data³¹ on A302, grade B, reported at the 5th Annual Information Meeting, have been rearranged in terms of the stress intensity factor range ΔK_I vs da/dN . The stress intensity factor for the test conditions employed for the displacement-controlled fatigue tests were calculated using the measured peak loads and the crack length by means of Bowie's expression^{32,33} for a double-edge crack type of specimen. The results are plotted as a series of straight-line segments as shown on the right side of Fig. 2.32. The slope of each line seems to be nearly equivalent, but the segments do not converge to a single straight line. This is considered to be due either to the contribution of plastic strain or to drifting of the mean stress with increasing crack length during the displacement-controlled fatigue test. The line drawn through the solid circle in each segment represents the equi-mean stress condition in each run of the displacement-controlled fatigue tests. The slope of this line has a value of 4.

To confirm the above results, a series of load-controlled, tension-to-tension fatigue tests have been attempted in ambient air environment using the Japanese domestic A533, grade B material. The maximum tensile loads for these tests covered the range of 60 to 80% of the gross-yielding condition. The results, as plotted in Fig. 2.32, fall on a band having a slope equivalent to that for the displacement-controlled tests.

Figure 2.33 summarizes the results of displacement-controlled fatigue tests for different environmental conditions. All the data points have

³³W. F. Brown and J. E. Srawley, *Plane Strain Crack Testing of High Strength Metallic Materials*, ASTM Special Publication 410, p. 11, 1966.

Fig. 2.32. Plots of da/dN vs ΔK_I relationship for the results obtained under different loading conditions.

Fig. 2.33. Plots of da/dN vs ΔK_I for displacement-controlled tension-to-compression tests in various test environments. Material: A302 grade B with precracked weld overlay. Waveform: sinusoidal at 10 cpm.

been obtained by rearranging the results reported formerly. The slope of the da/dN vs ΔK_I curves for the aqueous environment, $n = 8$, is characteristically steeper than that of the ambient air tests.

Load-controlled fatigue tests of the HSST material at a constant mean stress are under way with specimens prepared with double-edge cracks 3 mm deep. The results available at the time of this report are summarized in Figs. 2.34 and 2.35. The results of the ambient air tests (open circles) and a limited number of high-temperature water tests (closed circles) seem to belong to the same scatter band within the range of stress intensity factors tested as shown in Fig. 2.34. On the other

Fig. 2.34. Results of load-controlled, tension-to-tension tests in ambient air and 260°C water. Material: HSST A533 grade B.

hand, the crack-growth rate in an aqueous environment has been observed to exhibit substantial differences as a function of temperature. A typical example is shown in Fig. 2.34.

The fatigue tests under load-controlled conditions are currently impeded by a technical difficulty in measuring the actual crack size in the high-temperature, high-pressure aqueous system without shutdown. Consequently, the measurement of da/dN is currently made by frequently interrupting the test to measure the crack length directly. An optical system capable of viewing the specimen through high-temperature water has been developed to overcome the measurement difficulty and expedite the performance of tests.

Fig. 2.35. Crack-growth curves obtained in different environments. Test: load-controlled, tension-to-tension condition. Material: HSST A533 grade B.

The influence of cycling frequency on the crack-growth rate, da/dN , is also being studied by performing a series of tests under load-controlled conditions. A considerable increase in da/dN was noted upon decreasing the frequency from 10 to 1 cpm, whereas no substantial change was detected for the variation between 10 and 100 cpm. No growth incubation has occurred in the tests with frequencies down to 0.5 cpm. The minimum frequency planned for these tests is 0.1 cpm, below which the duration of a single test becomes unrealistic.

Summary and Conclusions

The incubation of crack growth observed in the fatigue tests in a high-temperature aqueous environment was found to occur under limited conditions, and the factors affecting such a trend were shown to be common with those causing pitting corrosion at the tips of growing fatigue cracks.

An apparent discrepancy has been observed based on the tests to date between the fatigue-crack-propagation characteristics for the displacement-controlled tension-to-compression tests and the load-controlled tension-to-tension tests with respect to the effect of a high-temperature water environment.

3. INVESTIGATIONS OF IRRADIATED MATERIALS

One of the environmental factors that must be considered in safety evaluations of reactor pressure vessels is irradiation, because the mechanical properties of metals may be modified thereby to a degree that is of considerable engineering significance. Irradiation effects were investigated only by Hanford Engineering Development Laboratory (HEDL); however, research programs are being planned at Westinghouse Electric Corporation and Oak Ridge National Laboratory. The areas of primary interest to the HSST program are the temperature range 450 to 600°F and the fast-neutron ($E > 1$ MeV) fluence range 1 to 8×10^{19} neutrons/cm².

In this chapter the research performed by HEDL is summarized together with that performed as a cooperative effort by Kernforschungsanlage GmbH, Julich, Germany. A summary of lower-bound toughness results for an irradiated steel is presented.

IRRADIATION EFFECTS ON THE FRACTURE OF HEAVY-SECTION PRESSURE VESSEL STEELS¹

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Hanford Engineering Development Laboratory

Objective

The determination of the embrittling effects of irradiation on pressure vessel steel has been the primary objective of the Hanford Engineering Development Laboratory work in support of the HSST program.

¹Research sponsored under Purchase Order No. 11Y-50917V between Union Carbide Corporation and Hanford Engineering Development Laboratory.

Characterization of irradiation embrittlement of ASTM A533, grade B, class 1 steel is being studied using plane strain fracture toughness K_{Ic} measurements. Tensile studies of irradiation strengthening have been conducted to determine valid application of fracture mechanics testing and to determine the range of irradiation strengthening sensitivity of A533 B. Tests are being conducted on compact-tension specimens to determine the applicability of K_{Icd} in evaluating lower-bound fracture toughness at high temperatures. Subcritical crack growth is being evaluated with fatigue-crack-propagation studies to determine if such properties are adversely affected by irradiation.

Effect of Strain Rate on the Tensile Behavior of ASTM A533 B

The tensile strain rate sensitivity of A533 was investigated over the strain rate range of 6.7×10^{-5} to 100 sec^{-1} and a temperature range of -319 to 500°F . Miniature buttonhead tensile specimens with a gage diameter of 0.125 in. and a gage length of 1.125 in. were used; the gage section diameter was tapered a maximum of 0.0015 in. from the ends toward the center of the gage length. Testing was conducted on an electrohydraulic closed-loop high-strain-rate tensile testing machine.

The yield strength of A533 for the various strain rates and test temperatures are given in Table 3.1. Data at the lowest strain rate are taken from curves previously reported.² Only the yield strength is reported, since other properties such as ductility, strain hardening, and ultimate tensile strength in the higher strain rate tests are specimen dependent due to the effects of adiabatic heating. Therefore, comparison of high strain rate plastic properties in the temperature-sensitive (thermal) region with those at lower strain rates or higher temperatures (athermal) would be questionable.

The data of Table 3.1 were parametrically reduced and are presented in Fig. 3.1. Bennett and Sinclair³ have verified the parameter based on

²J. A. Williams, "Irradiation Effects on Fracture of Heavy Section Pressure Vessel Steels," *HSST Program Semiannu. Progr. Rep. Aug. 31, 1971*, ORNL-4764.

³P. E. Bennett and G. M. Sinclair, "Parameter Representation of Low-Temperature Yield Behavior of Body-Centered-Cubic Transition Metals," *J. Basic Eng., Trans. ASME, Ser. D*, 88(2) (June 1966).

Table 3.1. Yield strength of ASTM A533 B HSST plate 02
at various strain rates and temperatures

Specimen No.	Test temp. (°F)	Strain rate (sec ⁻¹)	Yield strength (ksi)
a	-250	6.7×10^{-5}	105
a	75	6.7×10^{-5}	67
02GA715	-250	0.01	131.3
02GA716	-100	0.01	88.6
02GA360	70	0.01	74.0
02GA691	71	0.01	74.3
02GA677	71	0.01	73.0
02GA721	72	0.01	68.8
02GA192	72	0.01	68.7
02GA712	-250	0.1	124.8
02GA713	-100	0.1	88.5
02GA693	71	0.1	68.2
02GA748	500	0.1	65.0
02GA704	-250	1.0	146.3
02GA680	-100	1.0	95.6
02GA688	71	1.0	75.1
02GA679	500	1.0	61.0
02GA708	-321	10	174.8
02GA703	-250	10	160.2
02GA163	72	10	84.2
02GA246	72	10	81.1
02GA346	72	10	83.2
02GA347	72	10	79.8
02GA358	71	10	82.5
02GA359	71	10	86.7
02CA717	71	10	87.8
02GA697	72	10	84.6
02GA698	70	10	88.6
02GA709	500	10	67.2
02GA699	70	100	97.6
02GA702	70	100	101.6

^aSee Fig. 3.6 of Ref. 2.

the Arrhenius rate equation and expressed in the form

$$\ln \dot{\epsilon} = (-H/R) 1/T + \ln A, \quad (1)$$

where

$\dot{\epsilon}$ = strain rate,

H = apparent activation energy,

Fig. 3.1. Rate-temperature parametric representation of the yield strength of ASTM A533, grade B, class 1, 12-in.-thick steel plate.

T = absolute temperature,

R = universal gas constant,

A = frequency factor.

Based on the assumption that the activation energy is dependent only on the stress, then

$$T \ln A/\dot{\epsilon} = H/R = \text{constant} \quad (2)$$

for a given stress level. Bennett et al.³ have established the value of A to be about 10^8 for iron-carbon alloys.

The valid expression of yield strength through a parametric relationship will aid in a better definition of irradiated yield strength properties; these studies are currently in progress but are limited in scope due to the number of specimens available at each irradiation condition.

Fatigue-Crack Propagation of Irradiated ASTM A533 B

The propagation of fatigue cracks in irradiated ASTM A533, grade B, class 2 steel is currently being investigated. The specimens employed are 7/8T WOL specimens that have been machined by ORNL from the broken

halves of 2-in.-thick compact-tension specimens used to characterize the fracture toughness of irradiated material. Fatigue cycling is accomplished in an electrohydraulic closed-loop testing machine at a cyclic frequency of 600 cpm (10 Hz) and at a cyclic stress ratio of $R \approx 0$. Crack lengths are monitored periodically with a traveling microscope through a leaded glass window.

The results obtained to date are shown in Fig. 3.2. Also shown for comparison are results of Steele et al.⁴ at the Naval Research Laboratory (NRL), Clark⁵ at Westinghouse Research and Development Laboratory (WR&DL) for similar unirradiated material, and results from the NRL⁴ for irradiated material. Before discussing the effects of neutron irradiation, it is appropriate to point out what appears to be an effect of crack orientation. It will be noted that the unirradiated material in the WR orientation (NRL results) exhibits somewhat higher crack growth rates than the unirradiated material in the RW orientation (WR&DL results) at low values of ΔK . Similarly, in the irradiated material (NRL), cracks tend to propagate faster in the WR orientation than in the RW orientation. (In the WR orientation, the crack propagates parallel to the direction of rolling, while in the RW orientation, it propagates perpendicular to the rolling direction.) This orientation effect is consistent with the results of Heiser and Hertzberg⁶ for hot-rolled ferritic steel, Crooker and Lange⁷ for 10Ni-2Cr-1Mo-8Co steel, and James⁸ for 20% cold-worked type 316 stainless steel.

⁴L. E. Steele et al., *Irradiation Effects on Reactor Structural Materials, Quarterly Progress Report 1 May-31 July 1971*, NRL Memorandum Report 2328, pp. 12-17 (August 1971).

⁵W. G. Clark, Jr., "Effect of Temperature and Section Size on Fatigue-Crack Growth in Pressure Vessel Steel," *J. Mater.* 6(1), 134-49 (1971).

⁶F. A. Heiser and R. W. Hertzberg, "Anisotropy of Fatigue Crack Propagation," *J. Basic Eng.* 93(2), 211-17 (1971).

⁷T. W. Crooker and E. A. Lange, "Corrosion-Fatigue Crack Propagation Studies of Some New High-Strength Structural Steels," *J. Basic Eng.* 91(4), 570-74 (1969).

⁸L. A. James, "Fatigue Crack Growth in 20% Cold Worked Type 316 Stainless Steel at Elevated Temperatures," ANS Topical Meeting on Reactor Materials Performance, Apr. 24-26, 1972, Richland, Wash.

Fig. 3.2. Fatigue-crack propagation behavior of ASTM A533, grade B, class 1 steel at room temperature.

The preliminary results shown in Fig. 3.2 indicate that, at the lower values of ΔK , neutron irradiation tends to increase the fatigue-crack growth rate. This may be seen by comparing the HEDL results with the unirradiated WR&DL results for the RW orientation or by comparing the NRL results for either the RW or WR orientations with their unirradiated

counterparts. At higher values of ΔK , irradiated and unirradiated test results tend to converge.

The results reported here are for the first of seven specimens to be tested at temperatures of 75°F (24°C) and 550°F (288°C) and at fluences of 2.3 to 2.8×10^{19} and 5.3 to 5.7×10^{19} neutrons/cm² ($E > 1$ MeV).

Testing of Compact-Tension Specimens for Lower-Bound Toughness K_{Icd}

Irradiated and unirradiated Charpy-thickness compact-tension specimens (C_V CT) were tested as part of a cooperative effort with ORNL to determine lower-bound values of high-temperature fracture toughness. Unirradiated specimens were tested from -50 to 200°F; eight specimens with fatigue-cracked notches and eight with machined notches were oriented in the RW direction. Irradiated specimens with fluences of 1.8 to 2.3×10^{19} neutrons/cm² ($E > 1$ MeV) were tested between -120 and 550°F; specimens were machined by ORNL from HEDL irradiated 1T specimens, 15 in the RW orientation and 10 in the WR orientation. Irradiation test results are currently being analyzed by ORNL and HEDL and are summarized below.

Lower-Bound Toughness of Irradiated A533, Grade B, Class 1 Steel

F. J. Witt	J. A. Williams
ORNL	HEDL

Two series of specimens were machined from broken halves of irradiated specimens previously tested by HEDL. These prior results are presented in Ref. 9. The specimens were all tested statically. The load-displacement data were interpreted as outlined by Witt and Mager.¹⁰ A summary of the results is given in Table 3.2.

⁹F. J. Witt, *HSST Program Semiannu. Progr. Rep. Aug. 31, 1970*, ORNL-4653, pp. 33-35.

¹⁰F. J. Witt and T. R. Mager, *A Procedure for Determining Bounding Values on Fracture Toughness K_{Ic} at Any Temperature*, ORNL-TM-3894 (in publication).

Table 3.2. Summary of results from Charpy-thickness compact-tension (C_V CT) specimens machined from broken halves of 1T specimens

Reference drawings: ORNL 10476R-457, -458, -459, -460

Specimen No.	Test temp. (°F)	Fluence ^a ($\times 10^{19}$ neutrons/cm ²)	K_{IcC_v} (ksi $\sqrt{\text{in.}}$)	Origin ^b		
				1T CT specimen No.	Test temp. (°F)	K_{Ic} or (K_{IcI}) (ksi $\sqrt{\text{in.}}$)
<u>Longitudinal orientation</u>						
02G 0001	120	1.83	54.3	02GA4(A) ^c	120	47.0
0002	120	1.83	48.2	02GA4(B)	120	47.0
0003	-120	1.85	28.9	02GA10(A)	-120	27.6
0004	550	1.85	147.0	02GA10(B)	-120	27.6
0005	26	1.82	36.7	02GA22(A)	26	41.8
0006	450	1.82	166.0	02GA22(B)	26	41.8
0011	175	1.92	74.5	02GA2(A)	175	(84.3)
0012	175	1.92	75.7	02GA2(A)	175	(84.3)
0013	200	1.92	98.0	02GA2(B)	175	(84.3)
0014	250	1.92	131.5	02GA2(B)	175	(84.3)
0015	145	1.85	56.8	02GA3(A)	145	(69.5)
0016	145	1.85	55.2	02GA3(A)	145	(69.5)
0017	300	1.85	146.0	02GA3(B)	145	(69.5)
0018	350	1.85	158.0	02GA3(B)	145	(69.5)
<u>Transverse orientation</u>						
02G 0009	26	2.28	48.6	02GA24(A)	-100	40.5
0010	120	2.28	57.5	02GA24(B)	-100	40.5
0019	145	1.85	73.5	02GA1(A)	85	43.5
0020	175	1.85	105.5	02GA1(A)	85	43.5
0021	200	1.85	74.0	02GA1(B)	85	43.5
0022	250	1.85	130.0	02GA1(B)	85	43.5
0023	300	1.50	159.0	02GA21(A)	Not tested, broken in fatiguing	
0024	350	1.50	145.5	02GA21(A)		
0025	450	1.50	165.1	02GA21(B)		
0026	550	1.50	130.4	02GA21(B)		

^a $E > 1$ MeV, irradiated at 540 $^{\circ}$ F.

^bThe 1T specimens were longitudinally oriented.

^cThe letter in parentheses was used by ORNL to distinguish broken halves; HEDL's specimen numbers did not have this letter.

Seven of the longitudinally oriented Charpy-thickness compact-tension specimens were tested at temperatures corresponding with the test temperatures of the specimens from which they were machined. These tests are some of the validation tests for the lower-bound procedure. Once these data were obtained, tests were performed at temperatures up to 550°F. These data are presented in Fig. 3.3 and are compared with the similar results from the LT compact-tension specimens.

Fig. 3.3. Comparison of K_{Ic} and K_{IcI} with K_{IcC_v} for irradiated steel (nominally 2×10^{19} neutrons/cm², $E > 1$ MeV, irradiated at 540°F).

The transversely oriented specimens were tested over the same temperature range. Figure 3.4 gives a comparison of results for the two orientations. Essentially the static lower-bound toughness in Charpy thickness is the same regardless of orientation.

Additional testing is in progress which involves thicker specimens as well. However, these preliminary results clearly demonstrate for the first time that the static toughness for an irradiated steel does reach high toughness levels exceeding 100,000 psi√in. at a temperature considerably below the operating temperature of water reactor nuclear vessels.

Fig. 3.4. Comparison of lower-bound fracture toughness values between irradiated longitudinally and transversely oriented Charpy-thickness compact-tension specimens, ASTM A533, grade B, class 1 plate.

Erratum, Radiation Hardening and Embrittlement in
ASTM A533, Grade B, Class 1 Steel

W. J. Stelzman

Due to an inadvertent error in plotting the unirradiated data, the effect of irradiation on energy parameters determined in instrumented Charpy tests on irradiated and unirradiated HSST submerged-arc weldment 51B was incorrectly presented in Fig. 3.17 of the last HSST progress report.¹¹ Figure 3.5 presents the data with a corrected plot of the unirradiated data.

¹¹W. J. Stelzman, *HSST Program Semiannu. Progr. Rep. Aug. 31, 1971*, ORNL-4764, p. 34.

Fig. 3.5. Effect of irradiation on energy parameters determined in instrumented Charpy test on irradiated and unirradiated HSST submerged-arc weldment 51B; WL-oriented specimens.

NEUTRON IRRADIATION EMBRITTLEMENT OF ASTM A533 B STEEL
IRRADIATED AT 300 AND 400°C TO FAST-NEUTRON FLUENCES
OF 1, 5, AND 10×10^{19} neutrons/cm² (REF. 12)

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Kernforschungsanlage Julich GmbH

On behalf of Verein Deutscher Eisenhüttenleute (VDEh), Kernforschungsanlage Julich GmbH is performing a pressure vessel steel irradiation program. The HSST program made available to the VDEh two sections of HSST plates 02 and 03 (ASTM A533, grade B, class 1) as reference material. The VDEh in return will supply the HSST program with irradiation results obtained from this steel.

Extent of VDEh Irradiation Program

Three types of low-alloy MnNiMoV and NiCrMo steels have been selected. The steels being investigated as base metal, weld, and heat-affected zone (HAZ) have the following nominal chemical composition (wt %):

MnNiMoV steel - 0.11C, 1.4Mn, 1.5Ni, 0.4Mo, 0.15V

NiCrMo steel - 0.15C, 0.34Cr, 3.2Ni, 0.4Mo

NiCrMo steel - 0.19C, 1.4Cr, 3.4Ni, 0.5Mo

Investigations and tests will include Charpy V-notch impact tests, tensile tests at room temperature and 350°C, Vickers hardness tests, and metallography tests.

Specimens will be irradiated in the heavy-water-moderated and -cooled Dido reactor in a fuel element position, at fluences of 10^{19} , 5×10^{19} , and 10^{20} neutrons/cm² ($E > 1$ MeV), and at temperatures of 300 and 400°C.

Figure 3.6 shows the irradiation rig, which essentially consists of central supporting tube, two furnaces, and a shield plug. In each furnace there is space for 12 samples. The samples are arranged in two layers of six specimens each. Each layer has an independently controlled heating system with two thermocouples. One additional thermocouple is located

¹²This work was sponsored by Verein Deutscher Eisenhüttenleute and performed by Kernforschungsanlage Julich GmbH and is reported under an informal cooperative agreement with the HSST program.

Fig. 3.6. Irradiation rig.

between each two layers. During irradiation the rig is adjusted in a thimble filled with static helium gas. The unit, rig plus thimble, is located in the center of a fuel element.

The fluence was determined with Ni flux monitors [$^{54}\text{Ni}(n,p)^{54}\text{Co}$].

HSST Reference Steel, ASTM A533, Grade B Irradiations

Figure 3.7 shows the plate sections VDEh received from the HSST program: section A from plate 02 and section J from plate 03. Section A is being used as base metal, and section J has been used for weldment.

Table 3.3 shows the chemical composition of the base metal and weld.

The welding procedure, the welding data, and the procedure of sample preparation were described in detail in the paper "State of Cooperative Efforts between the HSST Program and the Pressure Vessel Steel Irradiation Program of Verein Deutscher Eisenhüttenleute (VDEh)" presented at the 5th Annual HSST Information Meeting, March 25-26, 1971.

Fig. 3.7. Location of test sections in HSST plates (dimensions in millimeters).

Table 3.3. Chemical composition of HSST reference steel

	Chemical composition (wt %)													
	C	Si	Mn	P	S	N	Al	B	Co	Cr	Cu	Mo	Ni	V
Base metal	0.22	0.30	1.44	0.012	0.008	0.023	0.023	0.0005	0.01	0.12	0.12	0.51	0.63	0.01
Weld	0.09	0.39	1.36	0.020	0.004	0.010	0.022	0.012	0.041	0.20	0.10	0.51	1.08	0.03

Results obtained so far are presented in Table 3.4. In addition, results of impact tests are shown in Figs. 3.8 to 3.11.

All the irradiations of the program were successfully completed. The remaining postirradiation examinations will be completed by July 1972.

Fig. 3.8. Results of impact tests at 300 and 400°C irradiation temperature.

Table 3.4. Results of HSST reference steel tests

Parameter ^a	Test temp. (°C)	Irrad. temp. (°C)	10 ¹⁹ neutrons/cm ²					5 × 10 ¹⁹ neutrons/cm ²					10 ²⁰ neutrons/cm ²					Unirradiated				
			Base		Weld		HAZ tr.	Base		Weld		HAZ tr.	Base		Weld		HAZ tr.	Base		Weld		HAZ tr.
			long.	tr.	long.	tr.		long.	tr.	long.	tr.		long.	tr.	long.	tr.		long.	tr.			
$\sigma_{0.2}$ (kp/mm ²)	20	300	53.5	53.5	55.9		49.5	60.5					65.8	62.1	72.3		50.3	49.0	48.5	53.6		44.2
		400	50.2	49.2	54.8		47.1						49.5	49.2	51.6		45.7					
	350	300	45.2		49.7		43.0	51.3					54.6		60.0		50.8	42.4		46.3		37.4
		400	43.0		46.0		40.9						43.1		47.7		40.6					
σ_u (kp/mm ²)	20	300	69.6	69.3	66.0		64.2	74.8					77.7	73.9	77.2		70.9	65.3	65.0	63.2		60.6
		400	69.4	65.6	64.6		61.9						65.5	65.3	63.4		61.4					
	350	300	63.4		61.7		60.4	67.7					69.4		67.9		65.0	61.1		59.0		57.3
		400	61.3		59.1		59.0						60.7		59.7		57.6					
δ (%)	20	300	23.2	22.5	27.7		17.1	22.4					20.2	20.2	22.0		16.0	23.6	22.8	27.9		18.9
		400	24.7	22.4	28.0		19.1						23.8	22.1	27.0		18.7					
	350	300	19.7		21.4		19.2	17.4					18.7		20.0		15.9	20.0		21.7		17.3
		400	23.2		21.7		16.9						19.2		20.2		17.9					
ψ (%)	20	300	68.3	62.6	70.0		65.1	66.0					66.6	63.6	64.1		64.2	69.7	66.7	74.3		69.5
		400	70.7	62.6	72.4		68.5						68.2	65.2	70.5		69.4					
	350	300	62.0		65.1		68.5	56.4					54.8		63.1		57.7	66.6		68.3		63.0
		400	69.2		68.1		58.9						61.7		69.2		65.6					
HV 10 (kp/mm ²)		300	231	229		237	228	290		262	269		273	279		270	263	212	211		217	221
		400	216	221		233	231			236	239		223	227		230	229					
TT (°C) at 30 ft-lb		300	+28	+23		-13	-30	+123		+24	-13		+102	+107		+77	+17	-5	+1		-63	-60
		400	+7	+8		+4	-45			+24	-5		+34	+40		+50	-12					

^a $\sigma_{0.2}$: 0.2% strength; σ_u : ultimate tensile strength; δ : elongation; ψ : reduction in area;
 HV 10: Vickers hardness; TT: transition temperature.

Fig. 3.9. Results of impact tests at 300 and 400°C irradiation temperature.

Fig. 3.10. Results of impact tests at 300 and 400°C irradiation temperature.

Fig. 3.11. Results of impact tests at 300 and 400 $^{\circ}\text{C}$ irradiation temperature.

4. PRESSURE VESSEL AND PIPING INVESTIGATIONS

Although four additional 6-in.-thick flawed tensile specimens are scheduled for testing, none were scheduled for the current report period. Several 1/6-scale models of the 6-in.-thick specimens were tested.

Preparations for the first test of the intermediate vessel continued. Techniques for using strain gages under high pressure as required for internal instrumentation of the intermediate test vessels received considerable attention. Techniques were also developed for the controlled growth and monitoring of cracks for the intermediate test vessels.

Continued progress has been made on the procurement of the intermediate pressure vessels. Three of the six vessels on order received final inspection and were accepted.

Planning of additional areas of investigation under the Ductile Pipe Rupture Research Program was completed and work initiated.

TESTING OF 1- AND 6-IN.-THICK FLAWED TENSILE SPECIMENS¹

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Southwest Research Institute

One-Inch-Thick Flawed Tensile Specimens

The 1-in.-thick tensile specimens are 1/6-scale models of the 6-in.-thick specimens. They are tested in an exact scale replica of the grip

¹Research sponsored under UCCND Subcontract No. 3202 between Union Carbide Corporation and Southwest Research Institute.

plates used on the Southwest Research Institute 15×10^6 lb capacity tensile test machine to test the 6-in. specimens, so that the load distribution on the test section is duplicated in the 1-in.-thick specimens.

All 1-in. specimens tested to date have been longitudinal specimens. The first two tested were center material from HSST plate 03. All subsequent specimens have been center material from HSST plate 01. Also, the flaws in the first two specimens were sharpened by a fatigue crack induced by bending the specimen. The flaws in all subsequent specimens were sharpened by charging a brittle zone at the root of a machined notch with hydrogen.² The notches were placed in the specimens using electrical discharge machining. The brittle zone at the root of the notch was created by concentrated heating with an electron-beam welder. A 10% solution of H_2SO_4 was used as an electrolyte for hydrogen charging. All machining and precracking, except for the fatigue work, has been performed at ORNL. The precracks formed using the electron-beam welder will be referred to as EB cracks for simplicity.

Three specimens with EB cracks were tested as received. Since it was suspected that either residual hydrogen or residual stress remaining in the specimen as a result of the precracking technique could affect the test results, one specimen was stress relieved and a second was baked at 400°F for 2 hr to drive off residual hydrogen. There was no appreciable difference in the test results of these specimens, tested at 200°F, but all subsequent specimens have been baked at 400°F as a precautionary measure.

Figure 4.1 shows a 1-in. test specimen in the test fixture with clip gages attached. Six clip gages were used on the first specimens tested, but the number was reduced to four after it was decided that the hole drilled in the side of the specimen affected the strain distribution at the adjacent strain gages and that the strain gage information was more desirable than the data from the clip gages. A total of 16 strain gages were placed on each specimen. A summary of some of the pertinent test parameters is given in Table 4.1.

²D. A. Canonico and J. D. Hudson, "Technique for Generating Sharp Cracks in Low-Alloy, High-Strength Steels," HSST Program 5th Annual Information Meeting, Mar. 25-26, 1971, Paper No. 20.

Fig. 4.1. Test setup for the 1-in.-thick flawed tensile specimens.

Table 4.1. Tensile test data for 1-in.-thick specimens

Gage length, 4 in.; width, 3 in.;
gross area, 3 in.

Specimen No.	Test temp. (°F)	HSSR plate No.	Net area	Elastic limit load (kips)	Elastic limit stress (ksi)		Ultimate load (kips)	Ultimate stress (ksi)		Average strain at ultimate ^a (%)
					Gross section	Net section		Gross section	Net section	
<u>Longitudinal specimens</u>										
b	210	03	2.66	175	58.4	65.7	230.6	78.6	88.9	5.61
c	50	03	2.42	176	58.6	72.7	223.0	74.3	92.2	0.57
01H 2015	0	01	2.58	150	50.0	58.2	174.0	58.0	67.4	0.31
01H 2017	25	01	2.63	150	50.0	57.1	190.0	63.4	72.2	0.54
01H 2011	50	01	2.60	145	48.2	55.7	195.0	65.0	75.0	1.21
01H 2009	50	01	2.59	150	50.0	58.0	207.5	69.1	80.1	5.75
01H 2013	75	01	2.59	160	53.3	61.8	225.0	75.0	86.8	4.62
01H 2014	75	01	2.61	160	53.3	61.4	235.0	78.4	90.0	4.98
01H 2016	100	01	2.59	150	50.0	58.0	229.0	76.4	88.5	7.15
01H 2018	130	01	2.63	140	46.6	53.2	240.0	80.0	91.3	3.70
01H 2012	200	01	2.60	160	53.3	61.6	224.0	74.6	86.1	4.30
01H 2010 ^d	200	01	2.60	165	55.0	63.4	220.0	73.3	84.6	5.77
01H 2008	200	01	2.58	155	51.6	60.1	215.0	71.6	83.3	5.32
01H 2019	200	01	e	140	46.6		274.0	91.4		14.50
<u>Transverse specimens</u>										
01H 2006	-50	01	2.60	140	46.6	53.8	158.0	52.6	60.8	0.22
01H 2004	50	01	2.60	155	51.7	59.6	205.0	68.3	78.9	0.72
01H 2005	75	01	2.60	145	48.4	55.8	242.5	80.9	93.4	1.35
01H 2001	100	01	2.60	170	56.6	65.4	226.0	75.3	86.9	1.20
01H 2000	130	01	2.60	150	50.0	57.8	216.0	72.0	83.2	3.05
01H 2002	200	01	2.60	140	46.6	53.8	235.0	78.3	90.4	1.64
01H 2003	200	01	2.60	180	60.0	69.2	250.0	83.4	96.2	1.82
01H 2007	200	01	2.41	100	33.3	41.6	230.0	76.7	95.4	1.69

^aDisplacement gage reading across test section containing flaw divided by original test section length.

^bUnsharpened.

^cLarge notch with unusual shape.

^dStress relieved.

^eUnnotched.

Six-Inch-Thick Flawed Tensile Specimens

No tests have been performed since the Fall 1971 Progress Report. Four additional tests are planned.

INTERMEDIATE VESSEL TEST SUPPORT ACTIVITIES

R. W. Derby

Strain Gage Developments

Techniques for using strain gages under high pressure received considerable attention. Among the problems considered were: (1) seals, (2) lead throughs, (3) waterproofing, and (4) pressure effects. For both training and development the apparatus shown in Figs. 4.2 to 4.4 was fabricated.

The most important component of the apparatus is a 3-in.-ID autoclave mounted on a trunion (Fig. 4.3). Strain gage lead wires penetrate the high-pressure environment through the fitting shown at the left end of the vessel, and oil or water is pumped in through the high pressure tubing shown on the right of the figure.

Inside the vessel a cantilever beam is mounted in a cage (Fig. 4.4). A strain gage is mounted on the top and bottom of the beam near the root. A layer of wax waterproofing can also be seen in the figure.

The cage shown in Fig. 4.4 is attached to the head of the autoclave by a small steel strap. Note that two lead weights have been slipped over the end of the beam.

When completely assembled the apparatus allows the investigator to vary the load on the cantilever beam while the gage is under different pressures by simply rotating the autoclave in the fixture shown in Fig. 4.3. (Note the ropes and pulleys in the figure.) With this technique it was possible to demonstrate that the strain caused by a given change in load is independent of the pressure surrounding the gage. In other words, the gage factor is essentially independent of pressure. This conclusion was checked up to 35,000 psi.

PHOTO 79562

Fig. 4.2. Cage assembly for mounting of cantilever beam.

Fig. 4.3. Trunion-mounted autoclave vessel.

PHOTO 79560

Fig. 4.4. Instrumented cantilever beam installed in cage with high-pressure lead fitting.

In addition to the investigation of the effect of pressure on gage factor, the autoclave tests were used to show that both the sealing and waterproofing techniques were reliable. It was also found that the high surface roughness, No. 36 grit, used under the high elongation strain gages caused unacceptably large pressure effects. Thus for measuring high strains under high pressure a smoother surface, No. 100 grit, was used. (Normal strain gage procedure calls for No. 200 grit.)

Finally, a number of tests were made to select a cement and a procedure which would allow both the measurements of large strains (above 5%) and rapid gage installation. It was found that cyano acrylate could survive 8% strain at room temperature and could be applied more than three times as fast as cements requiring heat cures.

Crack Sharpening

The critical task in preparing for the intermediate vessel test is to grow an acceptable fatigue crack out of a notch in the wall of the vessel. The device shown in Fig. 4.5 is being used to test the high-pressure crack-growing technique on a forging prolongation.

An O-ring seal is held in place around the notch by an array of steel cables. High pressure, approximately 18,000 psi, is delivered cyclically to the notch by the small high-pressure pump mounted on the bench.

An ultrasonic transducer is moved along the surface of the cylinder in a reproducible manner by the small electric motor on the side of the cylinder. This transducer is used to determine the extent of fatigue crack growth. At the top of the photograph is a drip bottle which is used to keep a slow but continuous flow of oil across the surface traversed by the transducer. Keeping good uniform contact between the transducer and the specimen is a crucial detail in the procedure.

Equally as difficult as keeping good contact was the problem of sealing. Because of relaxation, the cables shown in Fig. 4.5 could only be made to work by continually tightening them. A better technique is shown in Fig. 4.6, where steel bands are shown in place of the cables. Note the workman turning the big wrench to tighten the jack bolts and bring the sealing surface flush against the cylinder. The oscilloscope used with the ultrasonics can be seen in the background.

Fig. 4.5. Demonstration test of crack sharpening and crack monitoring apparatus.

Fig. 4.6. Assembly of steel-banded crack sharpening apparatus.

PROCUREMENT OF INTERMEDIATE TEST VESSELS

C. E. Childress

Six intermediate test vessels, shown in Fig. 4.7, are being fabricated by Taylor Forge Company of Paola, Kansas. The shell courses for these were secured from the original ingots as shown in Fig. 4.8. The first production work was begun on vessel V-2. On completion of assembly welding, radiographic inspection of the welds (done in accordance with Par. N-624 of Section III) revealed rejectable indications in both circumferential seams. These welds were then repaired such that no rejectable indications could be found by radiographic inspection.

Subsequent ultrasonic inspection, done in accordance with the procedure specified in Par. N-625 of Section III, revealed a number of rejectable indications in both circumferential seams (two in the manway to shell seam, G-2). The rejectable areas in seam G-2 comprised some 50% of the total circumference. It is significant to note that one of these areas was examined ultrasonically to determine the plane of the defect. A ^{60}Co source was then positioned such that the radiation beam was parallel to the plane of the indication, and the resultant radiograph showed the indication very clearly.

Taylor Forge concluded that repairs to seam G-2 (vessel V-2) would be so extensive as to render such a procedure almost impractical. They decided instead to remove this seam completely and reweld it.

In view of the difficulties encountered in welding the first vessel, Taylor Forge advised that they were considering modifying their submerged-arc welding procedure to include the following: (1) change from ac to dc; (2) decrease filler wire size from 3/16 to 5/32 in.; (3) lower amperage and voltage proportionately; (4) increase interpass temperature from 500 to 750°F; (5) increase intermediate PWHT temperature to that of the final PWHT temperature, that is, $1125 \pm 25^\circ\text{F}$; and (6) probably modify the joint design of the circumferential seams to provide for a wider opening. However, after experimenting with the proposed modification they ultimately decided to revert back to the original welding procedure.

To provide a completed vessel in the shortest possible time, it was decided to proceed with the fabrication of vessel V-1 while the bottom

Fig. 4.7. Orientation of shell courses for intermediate test vessels.

circumferential seam in vessel V-2 was being cut out and reprepared for welding. Vessels V-1 and V-2 are essentially identical.

On completion of assembly welding of V-1, radiographic inspection revealed three rejectable indications in seam G-2. It was necessary to

Fig. 4.8. Identification of shell courses by National Forge.

repair one of these areas a second time before the radiographs were acceptable. During fabrication the vessel and the attendant shell prolongation were subjected to 18 hr of PWHT. This, however, is well below the 28 1/2 hr of PWHT applied to the weld test specimens.

Longitudinal and shear wave ultrasonic inspections were performed on the two circumferential seams of the vessel and 7 in. on each side of the welds where accessible. Longitudinal wave inspection was performed to detect reflectors that may affect interpretation of the shear wave inspection.

Shear wave inspection was performed at a frequency of 2 1/4 MHz. The surface finish of the reference block, and the vessel in most cases, was better than 125 rms. No rejectable ultrasonic indications were observed.

The vessel was subjected to a hydrostatic pressure of 12,500 psi and allowed to stand for about 3 hr. During this time, no apparent leakage or loss in gage pressure was observed.

Prior to magnetic particle inspection, the vessel was dismantled and cleaned on both ID and OD surfaces. To eliminate surface burns, the magnetic field was induced with yokes rather than the prod method. No indications of defects were found by magnetic particle inspection.

After magnetic particle inspection the head assembly was reinstalled and the vessel was pressurized with 25 psig of N₂, 4 psig of He, and 21 psig of N₂, for a total of 50 psig.

The access nozzle was sealed and allowed to set for 2 hr. For this vessel, the calculations showed that the leak rate of the access nozzle was 9.4×10^{-5} std cc/sec, as opposed to the required leak rate of 5.8×10^{-5} std cc/sec. It was decided that the vessel would be acceptable even though the leak rate was slightly higher than specified.

Two other vessels, V-2 and V-3, have since been subjected to final inspection and found to be acceptable; V-3 contains a longitudinal seam. When this seam was first welded it contained rejectable indications, found by radiographic inspection, for the full length of the weld. This seam was cut out entirely and rewelded. In rewelding the longitudinal seams and welding of the two circumferential seams, no rejectable indications were found by radiography or ultrasonic inspection.

DUCTILE PIPE RUPTURE RESEARCH PROGRAM

An investigation has been initiated at Battelle Columbus Laboratories under UCCND Subcontract 3678 to extend their investigation of the extent of ductile pipe rupture.³ Emphasis will be placed on studying the tearing shear mode of fracture as observed in test 32 and reported in Ref. 3.

³*Investigation of the Initiation and Extent of Ductile Pipe Rupture, Final Report*, BMI-1908 (June 1971).