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Comments on the Leak-Before-Break Concept for Nuclear Power Plant Piping Systems

E. C. Rodabaugh

Work Performed for
U.S. Nuclear Regulatory Commission
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COMMENTS ON THE LEAK-BEFORE-BREAK CONCEPT FOR NUCLEAR POWER PLANT PIPING SYSTEMS

E. C. Rodabaugh

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TERMINOLOGY AND SYMBOLS

The following terms and symbols are used repeatedly in text and figures of this report. Other symbols are defined where used.

Failure	any fluid penetration across pressure boundary
Leak	fluid penetration across pressure boundary <50 gal/min
Break	fluid penetration across pressure boundary >50 gal/min
DEGB	double-ended guillotine break
c	one-half of crack length
d	crack depth
ℓ_f	crack length
M_b	bending moment
P	internal pressure
R	pipe cross section mean radius
R_b	bend radius of an elbow
S_m	*ASME Code* (Sect. III) allowable stress intensity
t	nominal pipe wall thickness
t_m	minimum pipe wall thickness; see Eqs. (12–14)
M_1	$[1 + 1.61(c/R)^2(R/t)]^{1/2}$
β	neutral axis location angle
θ	one-half of circumferential crack angle (c/R)
σ_a	axial stress due to internal pressure $(PR/2t)$
σ_b	bending stress $[M_b/(\pi R^2 t)]$
σ_f	flow stress, often taken as $(\sigma_u + \sigma_y)/2$
σ_h	hoop stress due to internal pressure (PR/t)
σ_m	axial membrane stress in Eqs. (8) and (10)
σ_u	material ultimate tensile strength
σ_y	material yield strength

FOREWORD

The work reported here was performed at Oak Ridge National Laboratory (ORNL) under the Heavy-Section Steel Technology (HSST) Program, C. E. Pugh, Program Manager. The program is sponsored by the Office of Nuclear Regulatory Research of the U.S. Nuclear Regulatory Commission (NRC). The technical monitor for the NRC is Milton Vagins.

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COMMENTS ON THE LEAK-BEFORE-BREAK CONCEPT FOR NUCLEAR POWER PLANT PIPING SYSTEMS

E. C. Rodabaugh

ABSTRACT

The leak-before-break concept is based on the idea that, with a high degree of probability, failure of the pressure boundary of piping systems will be signaled by a detectable leak that will provide ample time to shutdown and repair that leak. The status of the leak-before-break concept is discussed in this report, including a review of industrial and nuclear power plant experience with respect to leak-before-break, fracture mechanics, and potential elimination of postulated pipe breaks in nuclear power plant piping design.

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1. INTRODUCTION

The leak-before-break concept is based on the idea that, with a high degree of probability, failure of the pressure boundary of piping systems will be signaled by a detectable leak that will provide ample time to safely shutdown and repair that leak. If the concept is accepted in its entirety, three logical Nuclear Regulatory Commission (NRC) position changes would follow.

1. NRC requirements for postulated pipe breaks would be eliminated.
2. The existence, or possible existence, of cracks in piping (e.g., those caused by intergranular stress corrosion cracking) would be of no concern to NRC because they are not a safety hazard to the public.
3. Periodic in-service inspection of piping pressure boundaries (e.g., by ultrasonic examination) could be eliminated insofar as NRC is concerned.

Most readers will recognize that the leak-before-break concept is not sufficiently general and proven to warrant the implementation of all three of the NRC position changes.

This report consists of a review of the status of the leak-before-break concept. It starts with a discussion in Section 2 of the vague terms "leak" and "break" and an attempt to provide some simple conceptualization of leak rates. Section 3 contains a brief review of industrial piping experience, with an attempt to relate that experience to the leak-before-break concept by estimating break-to-leak ratios. Section 4 consists of a similar review of nuclear power plant piping experience.

Section 5 contains a brief review of fracture mechanics, which, in this report, is used as a generic heading for work that has been done on

the subject of load capacities of piping and piping components that contain cracks. Starting in about 1960 with investigations of axial cracks in natural gas transmission pipelines and continuing during the last 10 years at an accelerated pace, motivated in large part by intergranular stress corrosion cracking, there have been many hundreds of published papers and reports on the subject. We view our comments in this area as those of nonexperts. However, we have attempted to distill those portions of fracture mechanics work that appear particularly relevant to the subject of leak-before-break in nuclear power plant piping systems.

Section 6 contains a discussion and summary of our views on the status of the leak-before-break concept.

2. LEAK AND BREAK TERMINOLOGY

"Leak" and break" are vague terms and mean entirely different things to different people. Some conceptual stages in the spectra of leak/break follow:

1. L_1, a leak of a few drops of water per minute;
2. L_2, a leak that is detectable by nuclear power plant leak detection systems, generally 1 to 5 gal/min;
3. L_3, a leak that is equal to the normal makeup capacity:* for primary coolant, generally in the range of 50 to 200 gal/min for pressurized-water reactors (PWRs) and from 150 to 2000 gal/min for boiling-water reactors (BWRs);
4. L_4, a break that would permit leakage in excess of the normal makeup capacity; and
5. L_5, a double-ended guillotine break (DEGB). For small pipe and low fluid pressures, this might produce a leak rate less than L_3.

A visualization of leak rates is useful in connection with later discussions. If a kitchen faucet is turned on a little, so that the diameter of the water stream at 1 in. below the outlet is about 3/8 in., the leak rate is about 1 gal/min. For a piping system containing a flammable (e.g., gasoline) or toxic fluid, such a leak rate would be very hazardous and, in service failure reports, might be described as a rupture.

The leak rate L_1 can be visualized by assuming that a drop of water is a sphere with diameter of 0.2 in. The volume of the drop is then $(\pi/6)(0.2)^3 = 0.0042\ \text{in}^3$. A leak rate of 1 drop/s is then equivalent to 0.001 gal/min. Such a leak rate would be readily apparent to a casual passer-by and could be hazardous if the fluid were flammable or toxic. Nevertheless, it is three orders of magnitude less than the 1 gal/min leak detectability of nuclear power plant detection systems.

Table 1 shows leak rates of water and steam as a function of a given hole size in the pressure boundary. Of course, leaks in piping system pressure boundaries are generally through cracks of variable lengths and openings, but Table 1 gives a simple perspective between opening area and flow rates.

In subsequent portions of this commentary, we will need a term that encompasses all of the leak rates discussed above. We choose to use the term "failure," that is, any fluid penetration across the pressure boundary of piping. We will arbitrarily define a leak as <50 gal/min, a break as a leak >50 gal/min.

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*Coolant loss can be made up by nonemergency-core-cooling systems.

Table 1. Leak rates through sharp-edge orifice (hole in pressure boundary)[a]

Hole diameter, d_h (in.)	Water		Steam	
	P (psi)	L_w (gal/min)	P_i (psi)	L_s (gal/min)
0.1	10	0.57	25	0.012
	100	1.8	115	0.056
	1000	5.7	1015	0.49
	2500	9.0		
0.5	10	14.0	25	0.30
	100	45.0	115	1.4
	1000	140.0	1015	12.0
	2500	220.0		
1.0	10	57.0	25	1.2
	100	180.0	115	5.6
	1000	570.0	1015	49.0
	2500	900.0		

[a]Approximations for sharp-edge orifices:

Water: $L_w = 22.8\ (\pi/4) d_h^2\ P^{1/2}$, gal/min.
P = gage pressure inside pipe.

Steam: $L_s = 0.062\ (\pi/4) d_h^2\ P_i$, gal/min.
Critical flow assumed, saturated steam.
P_i = absolute pressure inside pipe.

3. INDUSTRIAL PIPING EXPERIENCE

This section discusses whether industrial piping experience has shown that leaks will probably occur before breaks. To defend the leak-before-break concept for nuclear power plant piping, it would be helpful to show that industrial piping experience indicates a very low probability of break before leak, for example, 0.001 or smaller.

3.1 National Board Incident Reports

The National Board of Boiler and Pressure Vessel Inspectors publishes a quarterly bulletin.[1] The April issues contain a table that summarizes incidents known to the National Board; Table 2 is taken from the April 1984 Bulletin and covers 1983 incidents. Tables in the same format are contained in the April 1983 and April 1982 Bulletins for 1982 and 1981 incidents, respectively. (In earlier bulletins, the data are published in a different format and are not as useful for our purpose.) In Table 2, the four lines identified as "Piping" and the three columns under "Type of Failure," labeled "Cracked," "Torn Asunders (rupture)," and "Leakage," have been identified as being relevant for our purpose. Assuming that "Cracked" and "Leakage" represent leaks and "Torn Asunders (rupture)" represents breaks, then the 1981–1983 incident reports can be summarized in Table 3.

A major aspect of the Table 3 assumptions is that "Torn Asunders (rupture)" were not preceded by leakage detectable by nuclear power plant leak detection systems, for example, 1 gal/min for one or more hours prior to the rupture. If they had been preceded by leakage, they should not be counted as breaks because, in principle, they would have been discovered and the plant shut down for repair prior to occurrence of the break. This question of leakage rates prior to breaks persists through Sects. 3 and 4. Considering the large amounts of escaping fluid associated with a 1-gal/min leak, our subjective judgment is that most of the failures classified in Sects. 3 and 4 as breaks were *not* preceded by leak rates exceeding 1 gal/min for 1 h or more.

Table 3 indicates that the probability of break before leak is about 0.3, not the 0.001 that would be more assuring for nuclear power plant piping. There are many reasons why these particular data are not applicable to nuclear power plant piping. Accordingly, we do not draw a positive inference but rather the negative conclusion; the National Board data do not support the hypothesis that the probability of break before leak is very low (e.g., 0.001).

3.2 Natural Gas Pipelines

Commencing February 9, 1970, all gas transmission companies were required to notify the Office of Pipeline Safety Operations (an office

Table 2. National Board 1983 Incident Report from April 1984 Bulletin[1]

INITIAL PART FAILURE	CAUSES							TYPE OF FAILURES							NUMBERS		
	Low Water Cut-off	Faulty Design Fab or Installation	Corrosion or Erosion	Operator Error or Poor Maintenance	Burner Failure	Pressure Control Failure	Other	Burned or Overheated	Collapsed Inward	Combination Explosion	Cracked	Torn Asunders (rupture)	Leakage	Others	Accidents	Injuries	Deaths
POWER BOILERS																	
Tube	125	18	114	82	12	8	75	165	23	10	83	64	197	48	465	3	1
Shell	18	2	44	27	0	0	29	22	1	1	43	4	31	6	97	0	0
Drum	1	3	36	35	3	8	0	6	0	0	3	0	22	0	21	23	0
Furnace	31	6	29	27	24	2	17	25	17	27	37	1	8	14	98	8	1
Tube Sheet	19	4	35	10	2	0	18	9	1	0	52	0	21	3	105	0	0
Header	2	3	8	5	0	0	2	5	0	0	6	0	17	2	16	0	0
Piping	1	0	25	4	2	0	12	0	0	2	7	7	49	0	27	2	0
Safety Valves	0	0	[illegible]	19	0	0	47	0	0	0	0	1	56	54	7	0	0
Misc	114	1	14	12	11	5	56	13	2	19	21	10	7	28	301	3	4
STEAM & HOT WATER STEEL HEATING BOILERS AND FIRED HOT WATER STORAGE TANKS																	
Tube	129	32	160	102	8	2	63	127	8	9	98	20	166	37	401	13	6
Shell	29	10	104	22	5	4	14	18	1	16	23	19	85	4	119	12	0
Drum	4	0	4	4	0	0	1	1	0	1	2	1	4	1	9	4	2
Furnace	229	7	39	21	155	1	13	15	6	24	53	3	8	5	315	1	2
Tube Sheet	17	17	40	22	1	1	6	13	5	0	51	2	16	0	59	4	0
Header	2	1	9	0	0	0	2	0	0	0	6	0	25	1	14	0	0
Piping	0	4	39	28	1	2	12	1	0	3	7	8	42	0	32	0	0
Safety Valves	0	0	17	1	0	0	98	0	0	0	0	5	67	61	8	0	0
Misc	7	2	10	6	11	5	32	11	3	16	16	3	27	21	94	1	3
CAST IRON BOILERS																	
Sections	562	51	340	109	17	8	128	141	1	22	1157	24	231	8	816	10	3
Tie Rods	0	0	0	0	0	1	0	0	0	0	0	1	0	0	216	0	0
Burners	1	0	0	1	16	0	1	3	0	6	0	1	1	1	14	0	0
Piping	0	0	29	8	0	2	7	0	0	0	7	4	34	1	23	0	0
Safety Valves	0	0	16	10	0	3	2	0	0	1	0	1	78	1	6	3	0
Misc	202	3	9	49	8	2	30	4	0	4	23	1	[illegible]	4	303	0	0
PRESSURE VESSELS																	
Shell	1	26	64	44	1	12	1143	15	13	1	61	31	41	22	167	75	13
Head	1	3	9	9	0	3	12	2	2	3	14	6	9	3	22	18	4
Attachments	0	2	3	2	0	2	2	0	0	0	15	3	16	4	19	2	0
Piping	1	7	14	54	2	6	24	1	0	1	16	18	72	4	62	16	0
Safety Valves	0	0	28	1	0	2	23	0	0	0	1	0	39	14	3	11	0
Misc	2	25	12	47	0	6	56	1	1	0	17	20	33	15	149	18	0

Table 3. Summary of National Board Incidents, 1981–1983

Year	Leaks	Breaks	Breaks/leaks
1981	55	14	0.25
1982	108	54	0.50
1983	197	37	0.19
Total	360	105	0.29

under the U.S. Department of Transportation) of a "reportable" incident. Reportable incidents are defined as those

1. resulting in a death or injury requiring hospitalization,
2. requiring the removal from service of any segment of transmission pipeline,
3. resulting in gas ignition,
4. causing an estimated damage to the property of the operator or of others or both of a total of $5000 or more,
5. involving a leak requiring immediate repair,
6. involving a test failure that occurred while testing with either gas or another test medium, or
7. involving an incident that, in the judgment of the operator, was significant even though it did not meet any of the above criteria.

Analyses of these data have been made by Battelle-Columbus Laboratories under sponsorship of the American Gas Association. Reports were issued in 1973, 1975, 1976, 1977, 1980, and 1984. Data from the 1984 report[2] are discussed in the following paragraphs.

The incidents are classified into ten types in Table 3 of Ref. 2. Types 1–5 are called "ruptures" by the pipeline operators; types 6–10 are called leaks by the pipeline operators. The ratio of ruptures to leaks is

$$1587/3021 = 0.53 .$$

This is a higher ratio than those derived from the National Board data and might be taken as indicative of the effect of high gas pressures and high-pressure-induced pipe stresses.

However, as noted in Sect. 2, the terms "leaks" and "ruptures" are vague. Reference 2 goes further and identifies those incidents in which the length of the fracture was given in the reporting form. Table 4 of Ref. 2 indicates 132 incidents where the fracture length was greater than 10 ft, 203 incidents where the fracture length was between 1 and 10 ft. This gives a ratio of breaks to leaks of

$$(132 + 203)/(4608 - 132 - 203) = 0.078 .$$

As in the case of the National Board data, there are many reasons why these particular data are not applicable to nuclear power plant piping. However, the data do not support the hypothesis that the probability of a break before leak is very low (e.g., 0.001).

While the preceding completes our major comments on leak-before-break clues from gas transmission piping data, other aspects are discussed in the following.

Table 6 of Ref. 2 indicates that there were 4293 incidents during 12 years of operation of steel piping systems. There was an average of 294,000 miles of piping in service during that time. In a nuclear power plant, we estimate there are about 5 to 10 miles of safety-related piping [primary coolant recirculation, safety injection, residual heat removal,

feedwater, component cooling water, chemical and volume control, portions of steam lines (e.g., to turbine-driven auxiliary feedwater pump), etc.]. Assuming 10 miles of such piping, and if that piping were equal in reliability to gas transmission piping, then the failure rate per plant year would be

$$\frac{4293}{294{,}000} \times \frac{10}{12} = 0.012 \text{ piping failures per plant year .}$$

(Recall that we have defined "failure" as any fluid penetration across the pressure boundary.)

Gas transmission pipelines are mostly buried and are subject to the hazard of an outside force, such as being struck by a bulldozer or backhoe. Approximately one-half of the gas transmission pipeline incidents are attributed to such "outside forces." Because nuclear power plant piping should not be subject to this particular hazard, another failure rate per nuclear power plant year is

$$\frac{1}{2} \times \frac{4293}{294{,}000} \times \frac{10}{12} = 0.006 \text{ piping failures per plant year .}$$

3.3 Power Utility Piping

General Electric's Report GEAP-4574 (Ref. 3) identified 399 failures in nonnuclear power utility piping. Of the 399 failures, 19 were classified as severance (rupture), leading to a ratio of breaks to leaks of

$$19/(399 - 19) = 0.050 .$$

Smith and Warwick[4] have compiled data on pressure vessels, including the number of defects and their distribution among the various methods by which they were found.

Method	Number of cases
Visual inspection	34
Leakage	49
Nondestructive testing	40
Catastrophic failure	16
Total	139

The ratio of breaks (catastrophic failure) to leaks is

$$16/49 = 0.33 .$$

In Sect. 4 we will be confronted with data in which there are N_1 reported incidents, N_2 of which are deemed to be breaks. Whether all of the incidents are failures is unknown. If we did not have the Ref. 4 breakdown by methods used to find the defects and assumed all defects were failures, the ratio of breaks to leaks would be

$$16/(139 - 16) = 0.13 .$$

4. NUCLEAR POWER PLANT PIPING EXPERIENCE

4.1 General Experience

WASH-1400 (Ref. 5) provides a summary of data of piping reliability as of that time (1975). However, with the exception of the General Electric data discussed in Sect. 3.3, there do not appear to be data relevant to our immediate concern; that is, given a failure, what is the relative probability of a leak or break?

Basin and Burns[6] evaluate data on 237 piping incidents that were reported in operating PWRs and BWRs from August 1960 to August 1976. They do not indicate whether all of the 207 incidents were failures as we have defined the term; however, it appears that most were based on this statement by Basin and Burns: "Twenty-two of the 237 reported pipe failures (i.e., approximately 9.3%) involved pipe ruptures, breaks or failures of such a nature so one would conservatively classify them as ruptures or breaks." This leads to a ratio of breaks to leaks of

$$22/(237 - 22) = 0.10 .$$

An effort is under way at Idaho National Engineering Laboratory to provide improved reliability estimates for nuclear power plant piping. Some preliminary results from Ref. 7 are discussed in this report. A literature search (e.g., Nuclear Power Experience, Nuclear Plant Reliability Data Systems, Licensee Event Reports, etc.) was conducted with primary focus on leakage rates of 50 gal/min or greater. However, because very few data points were available in that range, leakage rates of 2 gal/min or greater were included in the search. The preliminary results include 25 incidents.

If we could estimate how many failures have occurred in nuclear power plant piping up to and including those in 1983, we would have another break-to-leak ratio estimate. Reference 6 cites 237 incidents but also notes a very pronounced increase in the number of reported incidents per plant-year, from about 0.2 per plant-year in 1968 to 2.0 per plant-year in 1976. This trend appears to be even greater in recent years. For example, Ref. 8 summarizes data for 1980 and shows 363 reports concerned with piping and fittings. Noting that there were about 70 plants operating during 1980, the number of incidents per plant-year in 1980 was 363/70 ≈ 5.

Basin and Burns[6] suggest that the dramatic increase in piping incidents per plant-year is due to increased NRC reporting requirements. Indeed, they show that *all* reported incidents have increased at about the same rate as piping incidents. However, since about 1975, we think that reported incidents are being biased by intergranular stress corrosion cracking (IGSCC) cracks. To the extent that these cracks are indicated by ultrasonic testing (UT), they are not failures as we have defined the term; for example, the 363 incidents reported may include a significant portion of UT-indicated cracks. Unfortunately, we do not have a recent reference that would indicate what proportion of piping incidents are, in fact, failures. However, it appears that a piping failure rate of one

per plant-year is a reasonable estimate. At present, there are about 840 years of accumulated operation of nuclear power plants with an estimated 840 piping failures.

Returning to Ref. 7, preliminary compilation of incidents with leakage >2 gal/min and calling them breaks (even though most of them may have leak rates less than our definition of break ≥50 gal/min) lead to a break-to-leak ratio of

$$25/840 = 0.03 .$$

There have been at least three major breaks in 6-in. and larger nuclear power plant piping.

1. Indian Point 3, 1973 — An 18-in. feedwater line was cracked ~180° around the circumference. Fracture occurred inside containment at a fillet weld between pipe and containment penetration plate.
2. Turkey Point-3, 1972 — Main steam safety valve header split; three of four safety valves mounted on the header were blown off.
3. H. B. Robinson, 1970 — A 6-in. pipe nozzle between main steam line and safety valve failed completely (apparently a DEGB).

The large majority of failures are leaks in small-size lines. Quite often these failures occur in the pipe at the toe of socket welds, usually due to vibration-induced fatigue. However, occasionally a small line will sever completely, and a break (leak >50 gal/min) will occur. Usually, leak rates are not reported. However, in one case, a temperature detector line failed, leaving a 1/2-in.-diam opening in the primary coolant piping; the leak rate was about 130 gal/min. The reactor coolant system was being supplied at the maximum normal makeup flow rate of 100 gal/min. When the volume control tank level decreased to about 6%, a manual safety injection was initiated. The description of this failure emphasizes the significance of our definition of break and illustrates that failure of a small pipeline is not necessarily a leak.

4.2 Intergranular Stress Corrosion Cracking

Table 2.1 of Ref. 9 summarizes IGSCC incidents in U.S. and foreign BWRs. Before July 1975, there were 64 reported incidents; between July 1975 and January 1979, there were 69 reported incidents — a total of 133 reported incidents. Part of these incidents were found by leaking rather than by in-service UT inspection.

In March 1982, during a normal hydrotest, leaks were noticed at two of the furnace-sensitized Type 316 stainless steel 28-in.-diam recirculation loop safe ends. This motivated extensive additional UT inspections of BWR piping, using more sensitive UT methods than had been used in the past. Table 3.1 of Ref. 10 lists 365 cracked welds. This large number of cracked welds is, in large part, the result of using the more sensitive UT methods; it may turn out that many of the reported cracks are weld root irregularities. In any event, none of these "cracked welds" is a failure as we have defined the term.

To estimate the breaks-to-leak ratio for the specific case of IGSCC, we need the number of such incidents that were actual failures, for example, those discovered by occurrence of leakage. We do not have a compilation of such failures. In Refs. 9 and 10, several IGSCC failures are mentioned. For example, Ref. 10 notes that in Japan 43 welds were found to have IGSCC; 13 of the 43 were discovered by leakage. The Duane Arnold 12-in. and Nine Mile Point 28-in. safe ends IGSCC were discovered by leakage. A number of IGSCC incidents in recirculation loop bypass lines were also discovered by leakage. Our estimate is that about 100 IGSCC incidents (U.S. and foreign) have been discovered by leakage.

The only IGSCC crack for which leakage rate is available is for the Duane Arnold failure, a reported leak rate of 3 gal/min. We speculate that none of the estimated 100 IGSCC failures had leak rates much greater than 3 gal/min; hence (if our speculations are correct), there have been no breaks, but there have been about 100 leaks. This experience is different from that indicated by the break-to-leak ratios from general industrial and nuclear piping experience for which a break-to-leak ratio of about 0.03 or higher (3 breaks for 100 leakages) would be expected. The reasons for this difference are discussed in Sect. 5.

5. FRACTURE MECHANICS

Research work on axial cracks in gas transmission piping in the late 1950s and early 1960s led to simple methods for estimating the internal pressure that can be sustained by a pipe with axial cracks as a function of crack length and depth. These methods are discussed in Sect. 5.1.

In recent years, motivated in large part by intergranular stress corrosion cracking along girth butt welds in BWR piping, simple methods have been developed for estimating the axial load and bending moment capacity of pipes with circumferential cracks as a function of crack length and depth. These methods are discussed in Sects. 5.2—5.4.

The methods discussed in Sects. 5.1—5.4 rest on the assumption that the pipe material is such that crack extension is ductile. Test data indicate that wrought austenitic stainless steels (e.g., Types 304 and 316) are ductile in this sense. Wrought carbon steel such as A 106 grade B may also be ductile in this sense, but there is a minimum temperature and perhaps a maximum wall thickness for A 106 grade B pipe to be ductile.

The more versatile tearing instability analysis is discussed in Sect. 5.5. The Lawrence Livermore National Laboratory probabilistic evaluations are discussed in Sect. 5.6.

5.1 Internal Pressure, Axial Cracks

Figure 1 shows stable and unstable regions in straight pipe with axial cracks based on Ref. 11. We use the terms "stable" or "unstable" rather than leak or break because a failure in the stable region could involve a leak rate >50 gal/min that we have arbitrarily defined as a break. For example, if a surface crack with $c/R = 0.4$ grew to a through-wall crack with the same c/R and if the pipe were 28-in. diam ($R = 14$), then the crack length would be 11.2 in. With internal pressure, even a stable crack tends to open; in this case, probably not less than 0.5 in.; thus, the opening area would be not less than $11.2 \times 0.5/2 = 2.8$ in.^{2}. If the fluid were saturated steam at 1000-psi pressure, the leak rate would be about 200 gal/min. Much larger leak rates would occur if the fluid were water.

For shallow cracks (small d/t ratios), Fig. 1 indicates that a failure would be unstable. This agrees with burst tests on piping components with no intentional defect (d/t and/or $c/R = 0$). In burst tests, the component is capped and filled with water venting out the air, and then the pressure is increased until failure occurs. Figure 2 shows a typical burst test failure of a length of 12-in., 0.375-in. nominal wall, A 106 grade B pipe. Figure 3 shows a typical failure of a 6-in. branch connection in a 12-in., 0.375-in. nominal wall, A 106 grade B pipe. Figures 2 and 3 are from Ref. 12. The extent of the fracture depends upon the energy of the contained fluid and the crack-arresting properties of the material. If the burst tests had been run with air as the pressurizing fluid, the cracks in Figs. 2 and 3 would have extended much farther.

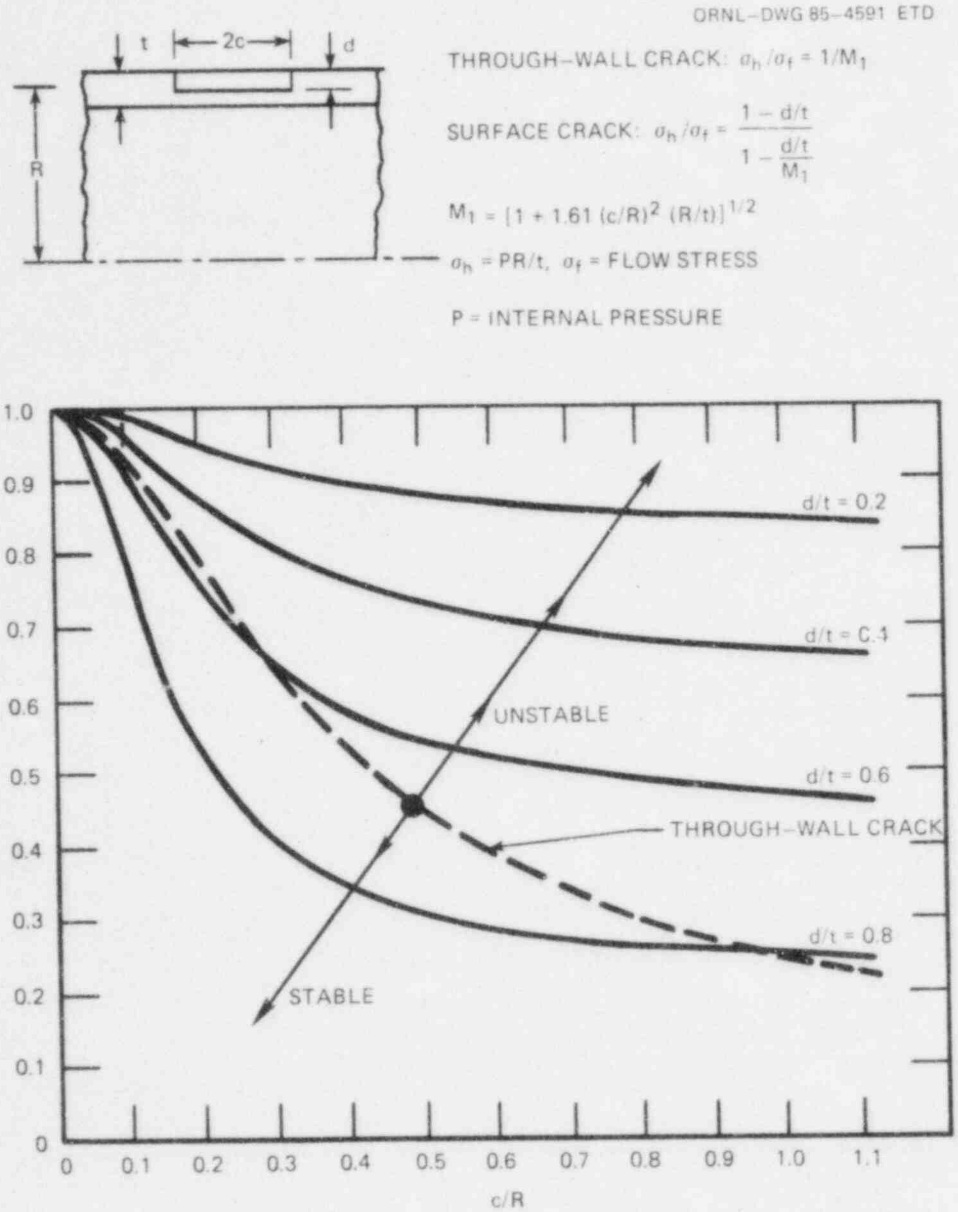

Fig. 1. Stable and unstable regions for axial cracks in straight pipe with R/t = 10, internal pressure loading.

The burst pressure of straight pipe with no cracks is well approximated by the equation:

$$P_b = t\sigma_u/R \,, \qquad (1)$$

where

t = wall thickness,
R = pipe mean radius,
σ_u = ultimate tensile strength of the pipe material.

ORNL–PHOTO 3370–85

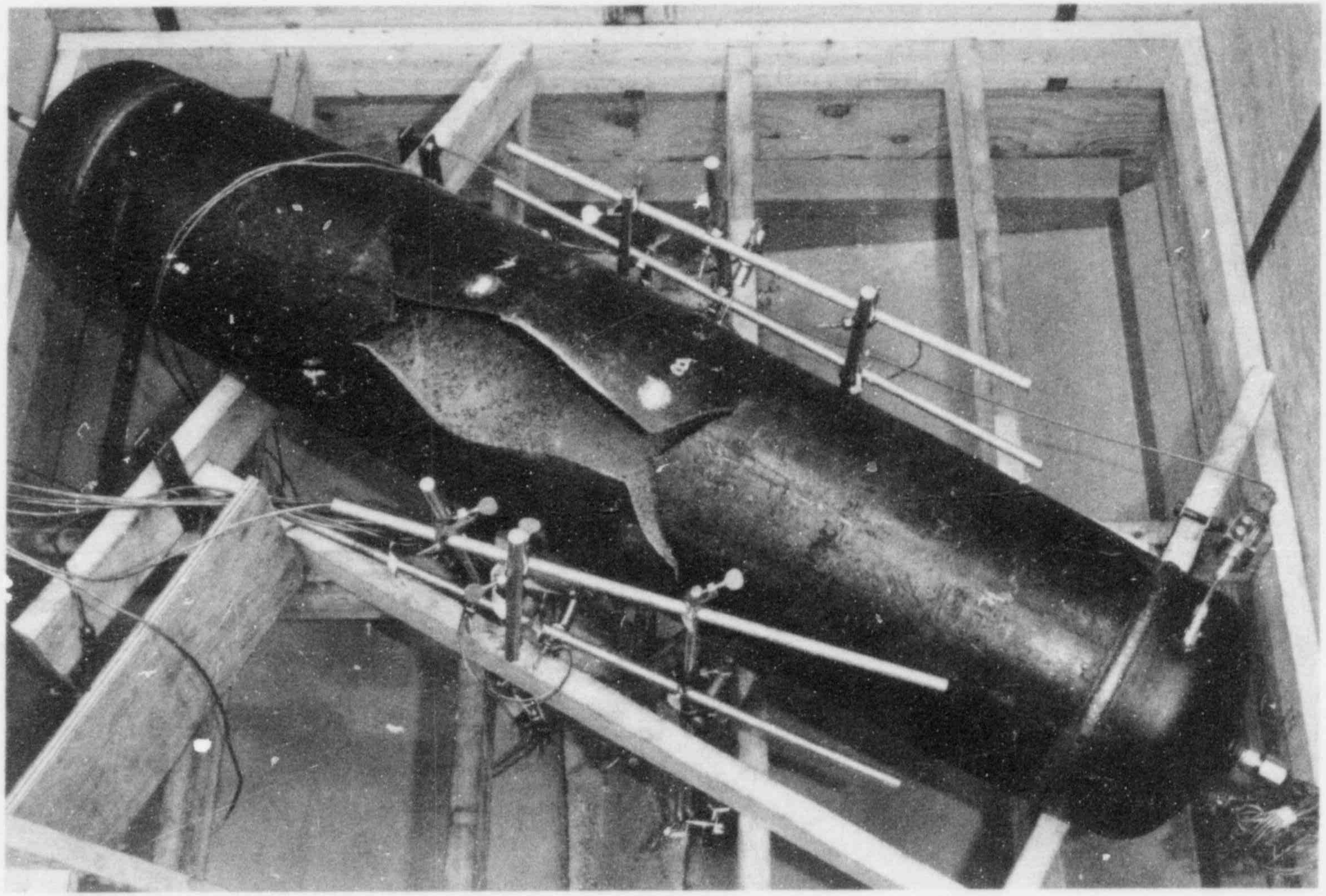

Fig. 2. Postburst test photo, 12-in. by 0.375-in. A 106 grade B pipe.

ORNL–PHOTO 3371–85

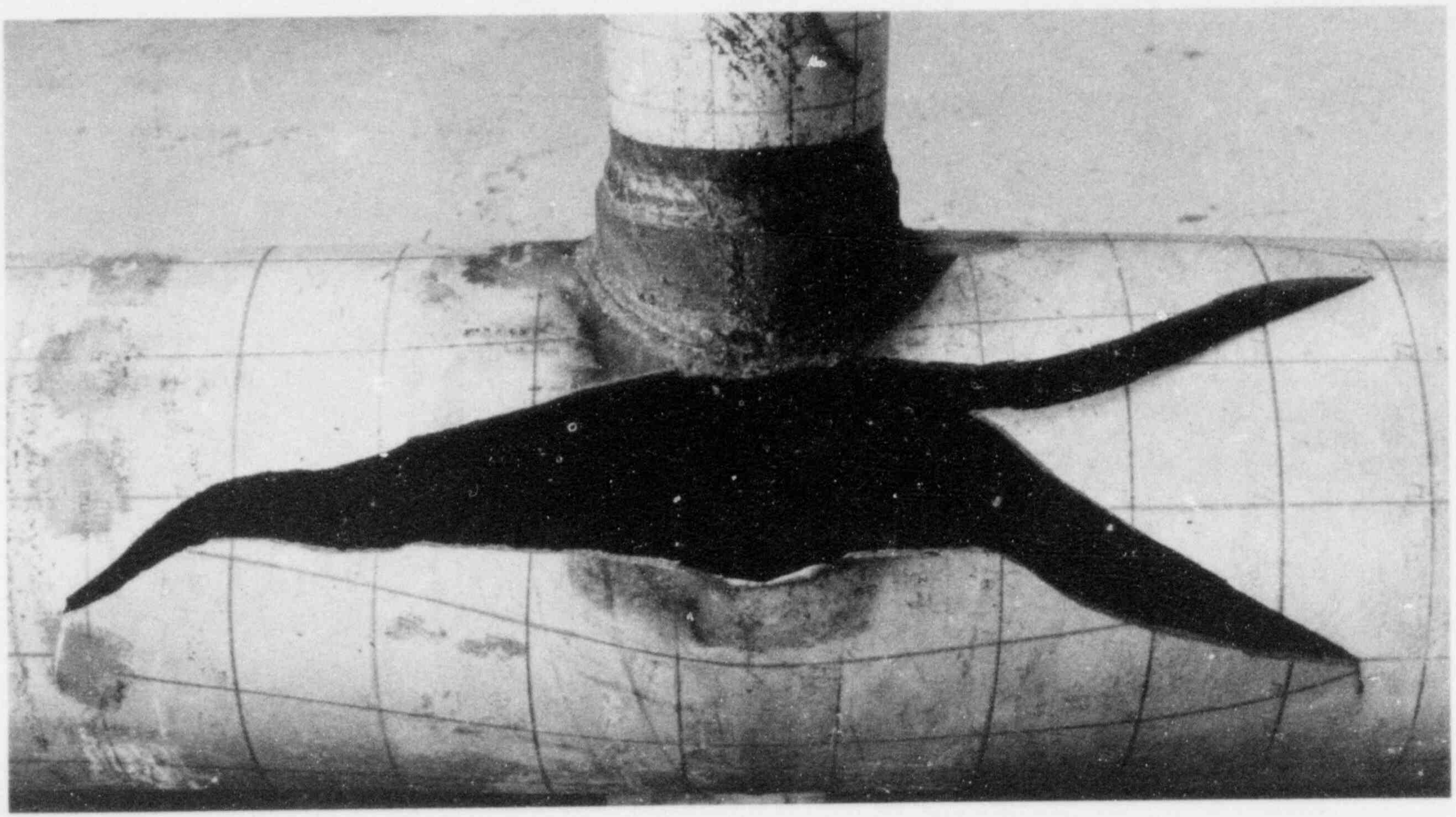

Fig. 3. Postburst test photo, 6-in. branch in 12-in. by 0.375-in. A 106 grade B pipe.

In Fig. 1, the burst pressure for $d/t = 0$ or $c/R = 0$ is given by

$$\sigma_h = PR/t = \sigma_f \ . \tag{2}$$

The flow stress σ_f is usually taken as $(\sigma_y + \sigma_u)/2$, where σ_y is the material yield strength. With this definition of σ_f or any definition of σ_f such that $\sigma_f < \sigma_u$, the prediction of failure pressure by Fig. 1 for "no crack" is incorrect but conservative.

The "no crack" type of break can occur in actual piping systems for reasons such as:

1. Overpressure: Water hammers or relief valves either are not used, are of inadequate capacity, or fail to operate or lines to the relief valves become plugged.
2. Reduced wall thickness: General corrosion and/or erosion occurs such that the wall thickness over a large area is reduced. This tends to occur at piping elbows.
3. Overtemperature: Temperature increases over design temperature. This can occur in furnace tubes by scaling or loss of fluid flow. In piping, exposure to a fire can cause such a break to occur.

While we do not have details of the 105 breaks summarized in Table 3, we suspect that many of them are of the "no crack" type. The 335 fractures longer than 1 ft, identified in Ref. 2, are deemed to be axial breaks like Fig. 2. Gas pipelines are operated at hoop stresses σ_h up to $0.72\ \sigma_y$ and tested with σ_h up to σ_y. The piping material is such that σ_u may be only about 30% higher than σ_y. Accordingly, at operating conditions:

$$\sigma_h/\sigma_f = 0.72\ \sigma_y/[(\sigma_y + 1.3\ \sigma_y)/2] = 0.63 \ . \tag{3}$$

At test conditions:

$$\sigma_h/\sigma_f = 1.00\ \sigma_y/[(\sigma_y + 1.3\ \sigma_y)/2] = 0.87 \ . \tag{4}$$

Figure 1 indicates that, even with $\sigma_h/\sigma_f = 0.87$, a failure would not be expected if there were no cracks. However, large-size gas transmission lines are made of pipe with longitudinal welds, and defects in these welds sometimes occur. It is apparent in Fig. 1 that relatively shallow defects could lead to an unstable failure.

Nuclear power plant piping operates with, at most, $\sigma_h = S_m$, where S_m is a *Code*-tabulated allowable stress intensity. The value of S_m is about 0.3 to 0.4 times the value of $\sigma_f = (\sigma_y + \sigma_u)/2$. Accordingly, Fig. 1 shows that a large value of c/R and d/t would be needed to produce an unstable failure. For example, at $\sigma_h/\sigma_f = 0.4$, R = 12 in., the crack would be about $0.57 \times 14 \times 2 = 16$ in., with a crack depth of about 0.7t.

Recently, tables of allowable axial crack sizes, based on the relationships shown in Fig. 1, were added to Section XI of the *ASME Code*.[13]

Specifically, it was assumed that $\sigma_f = 3S_m$ and factors of safety of 3 and 1.5 (Table IWB-3641-3, Normal and Upset and Test conditions, and Table IWB-3641-4, Emergency and Faulted Conditions, respectively[13]) were used. By using these factors of safety on pressure, the "unstable" region is partially avoided. In addition, the limit

$$(\sigma_h/\sigma_f) \leq 1/M_1 \tag{5}$$

was used. With $\sigma_f = 3S_m$, this leads to the limit

$$\ell_f/\sqrt{Rt} = 2c/\sqrt{Rt} \leq \left[\left\{[3/(\sigma_h/S_m)]^2 - 1\right\} \times 4/1.61\right]^{1/2} . \tag{6}$$

This limit is the reason for the abrupt cutoffs in Tables IWB-3641-3 and -4 (Ref. 13). Nevertheless, there are allowed crack dimensions that are in the unstable region of Fig. 1. For example, the combination from Table IWB-3641-3 of $\ell_f/\sqrt{Rt} = 5.0$ ($c/R = 0.790$) with $d/t = 0.14$ is well into the unstable region of Fig. 1.

5.2 Axial Loads, Circumferential Cracks

Figure 4 shows stable and unstable regions in straight pipe with $R/t = 10$, based on Ref. 14. A major source of axial stresses in piping systems is internal pressure; in which case, $\sigma_a = PR/2t$. By comparing Figs. 1 and 4, it appears that circumferential cracks tend to be more stable than axial cracks. In addition, of course, for pressure loading only $\sigma_a \approx \sigma_h/2$; hence, the possibility of any failure at all is much lower for circumferential than for axial cracks.

The other major source of axial stress in piping systems is a bending moment. The bending stress varies with location but, if the crack length is small (e.g., $c/R < \sim 0.5$) and the crack is located at the maximum nominal stress location, Fig. 4 should give some guidance for axial stresses caused by bending moments. This is discussed further in Sect. 5.3.

5.3. Bending Moment Loads, Circumferential Cracks

Figure 5 shows stable and unstable regions in straight pipe with circumferential cracks, based on Ref. 15. It can be seen that Fig. 5 is almost identical to Fig. 4. The difference lies in the value of M_2 in Fig. 4 and M_3 in Fig. 5; M_2 is from a theoretical development by Delale and Erdogan,[16] whereas M_3 is an empirical development based on test data as indicated in Ref. 15. M_2 is a function of $\lambda = (c/R)\sqrt{R/t}$; whereas M_3 is a function of $c/\pi R$. Obviously, they both cannot be generally accurate and indicate that simple methods of identifying stable/unstable regions for circumferentially cracked straight pipe with bending moments or internal pressure need further development.

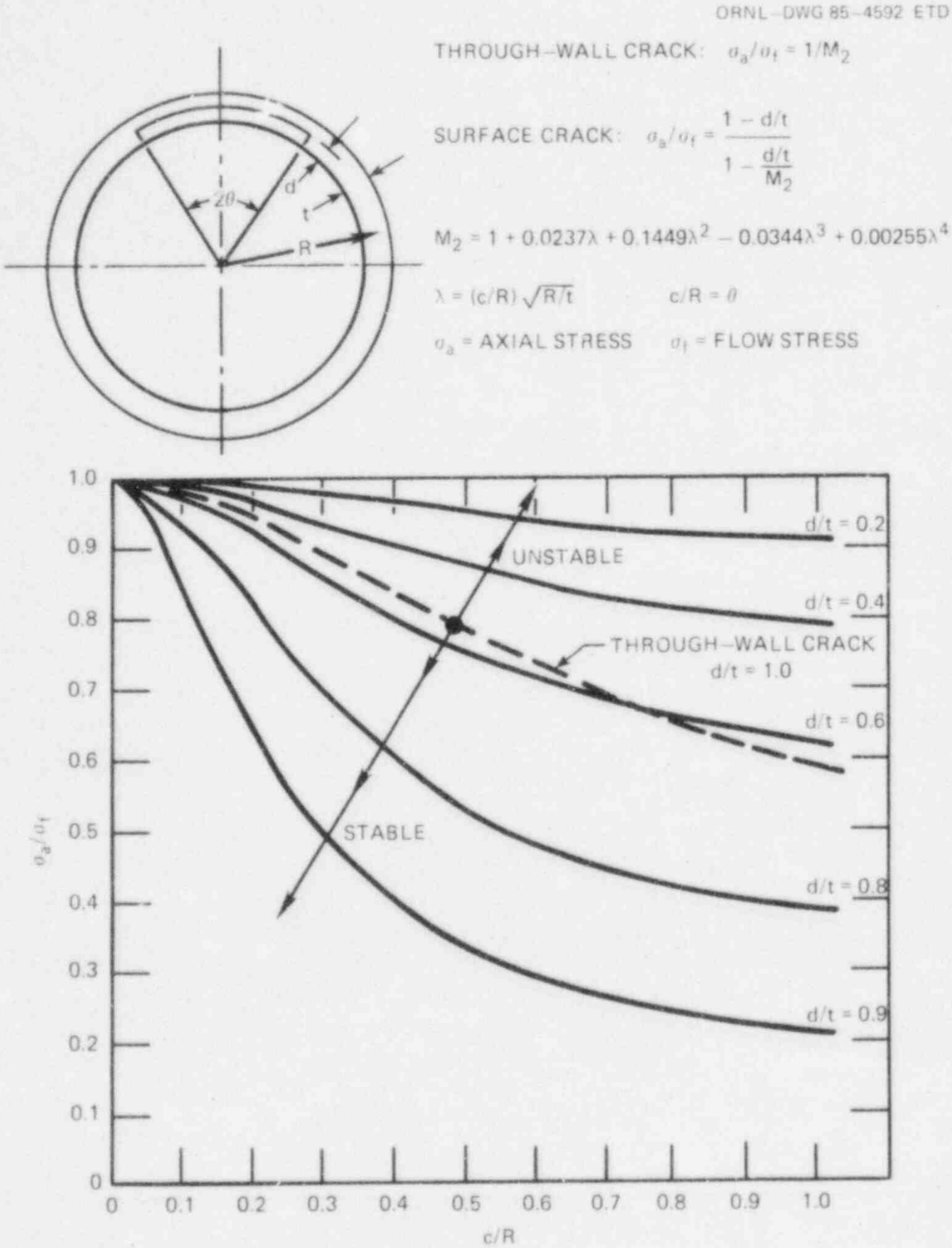

Fig. 4. Stable and unstable regions for circumfere *al cracks in straignt pipe with R/t = 10, axial loading.

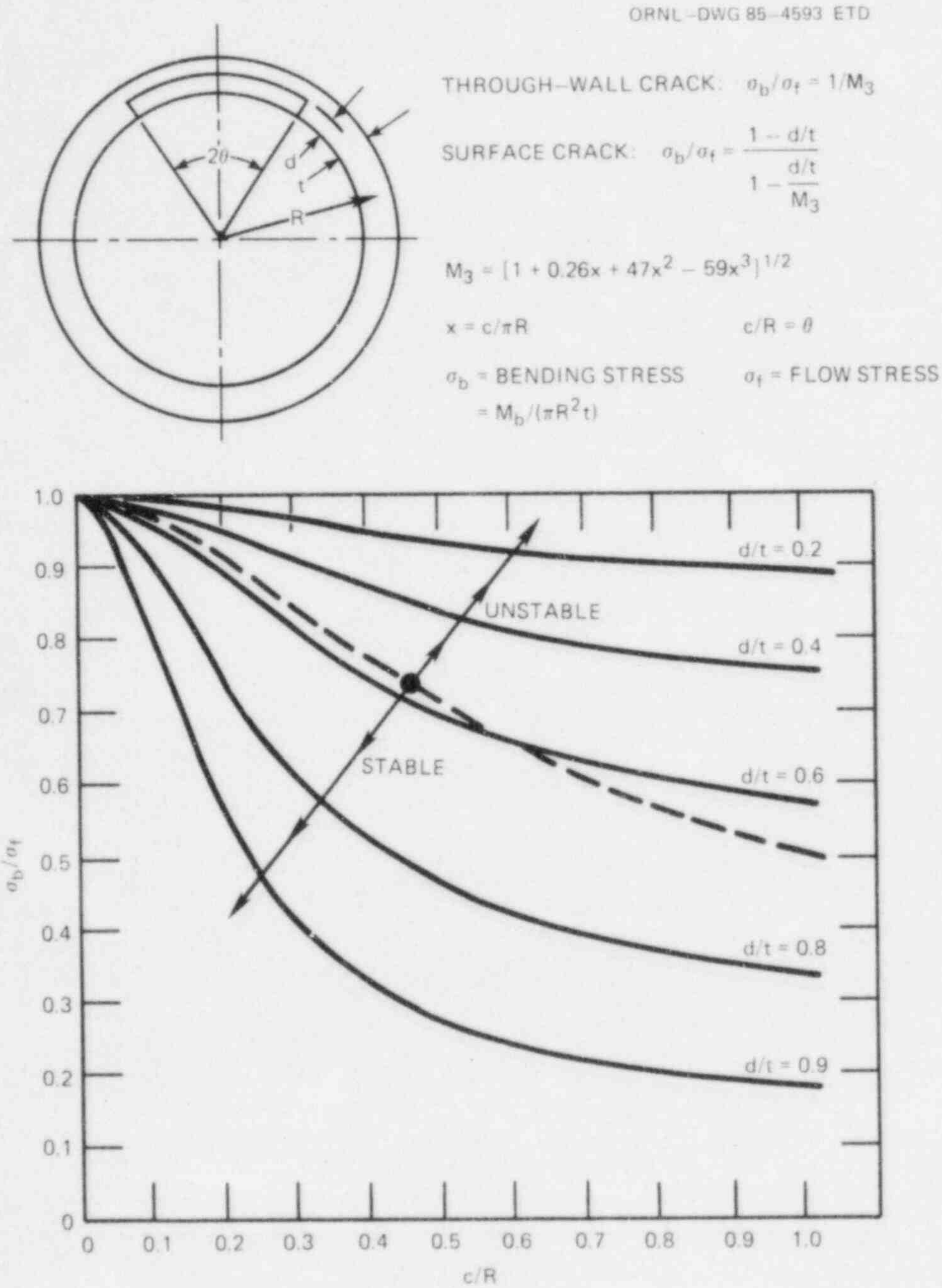

Fig. 5. Stable and unstable regions for circumferential cracks in straight pipe, bending moment loading.

5.4 Bending Moment and Axial Loads, Circumferential Cracks

Reference 17 includes a "net section collapse" analysis by the following equations.

For $(\beta + \theta) < \pi$:

$$\sigma_b/\sigma_f = (4/\pi)[\sin \beta - 0.5 (d/t)\sin \theta] , \tag{7}$$

$$\beta = (\pi/2)[1 - (\theta/\pi)(d/t) - (\sigma_m/\sigma_f)] . \tag{8}$$

For $(\beta + \theta) > \pi$:

$$\sigma_b/\sigma_f = (4/\pi)[1 - 0.5(d/t)]\sin \beta , \tag{9}$$

$$\beta = \pi[1 - (d/t) - (\sigma_m/\sigma_f)]/[2 - (d/t)] , \tag{10}$$

where

σ_b = bending stress = $M_b/(\pi R^2 t)$,
M_b = bending moment,
σ_f = flow stress,
σ_m = axial stress = $PR/2t$ for internal pressure loading,
P = internal pressure,
d, t, θ and R are defined in Fig. 5.

For comparison with Fig. 4, Eqs. (7) and (8)* with $\sigma_b/\sigma_f = 0$ give

$$\sigma_m/\sigma_f = 1 - (\theta/\pi)(d/t) - \sin^{-1}[0.5(d/t)\sin \theta]/(\pi/2) . \tag{11}$$

Equation (11) is shown in Fig. 6. Comparison between Fig. 4 and Fig. 6 indicates substantial differences, particularly for $d/t = 1.0$ (through-wall crack). However, Eqs. (7)–(10) are intended to represent maximum load capacity; whereas the through-wall crack line in Fig. 4 presumably represents the boundary between stable and unstable cracks. The net section collapse analysis predicts that surface crack ($d/t < 1.0$) failure stresses are always above the through-wall crack ($d/t = 1.0$) stresses; hence, all surface crack failures are predicted to be load-controlled instabilities.

For comparison with Fig. 5, Eqs. (7) and (8)* with $\sigma_m/\sigma_f = 0$ are shown in Fig. 7 (Ref. 17). A comparison of Fig. 5 and Fig. 7 indicates

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*For the range of c/R covered in Figs. 4, 5, and 6, $(\beta + \theta)$ is always less than π. Hence, Eqs. (9) and (10) are not involved.

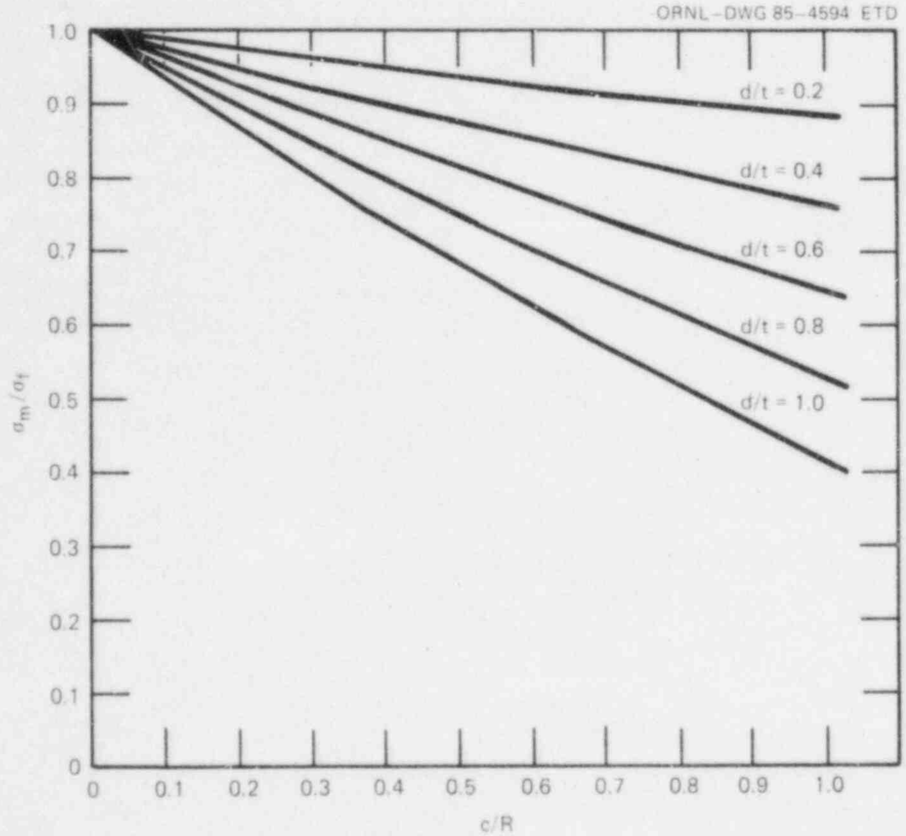

Fig. 6. Maximum load capacity of circumferential cracked straight pipe, axial loading [see Eq. (11)].

substantial differences, which are analogous to comparisons of Fig. 4 and Fig. 6. In addition, Fig. 7 is based mainly on stainless steel pipe test data; Fig. 5 is based on carbon steel pipe test data.

Equations (7)–(10) were used as the basis for allowable circumferential crack sizes in Tables IWB-3641-1 and -2 of the *Code*.[13] It was assumed that $\sigma_m = S_m/2$; hence, $\sigma_m/\sigma_f = 1.6$. Figure 8 (based on Ref. 17), shows the maximum load capacities σ_b/σ_f as a function of $(c/\pi R)$ and (d/t).

In BWR piping systems used as primary coolant boundaries (e.g., recirculation system), the axial stress due to internal pressure during normal operation is bounded by $\sigma_m/\sigma_f \leqslant 1/6$. Further, bending moment stresses are typically 10 ksi or less and, at 550°F operating temperature, $3S_m = 51$ ksi; hence σ_b/σ_f is typically <0.2. Looking at Fig. 8 in the region where $\sigma_b/\sigma_f \leqslant 0.2$, it is apparent that through-wall cracks with $(c/\pi R) \geqslant 0.4$ must exist for load capacity to be exceeded. For example, for a 12-in. pipe with R = 6, the through-wall crack length 2c must be $>0.4x \times 2 \times 6 = 15.1$ in. Reference 10, Table F.3, indicates that, $c/\pi R = 0.2$ will leak at a rate of 10 gal/min. Accordingly, IGSCC cracks would be expected to leak at sufficiently high rates in the stable region of Fig. 8 so that they would be detected by leakage prior to break.

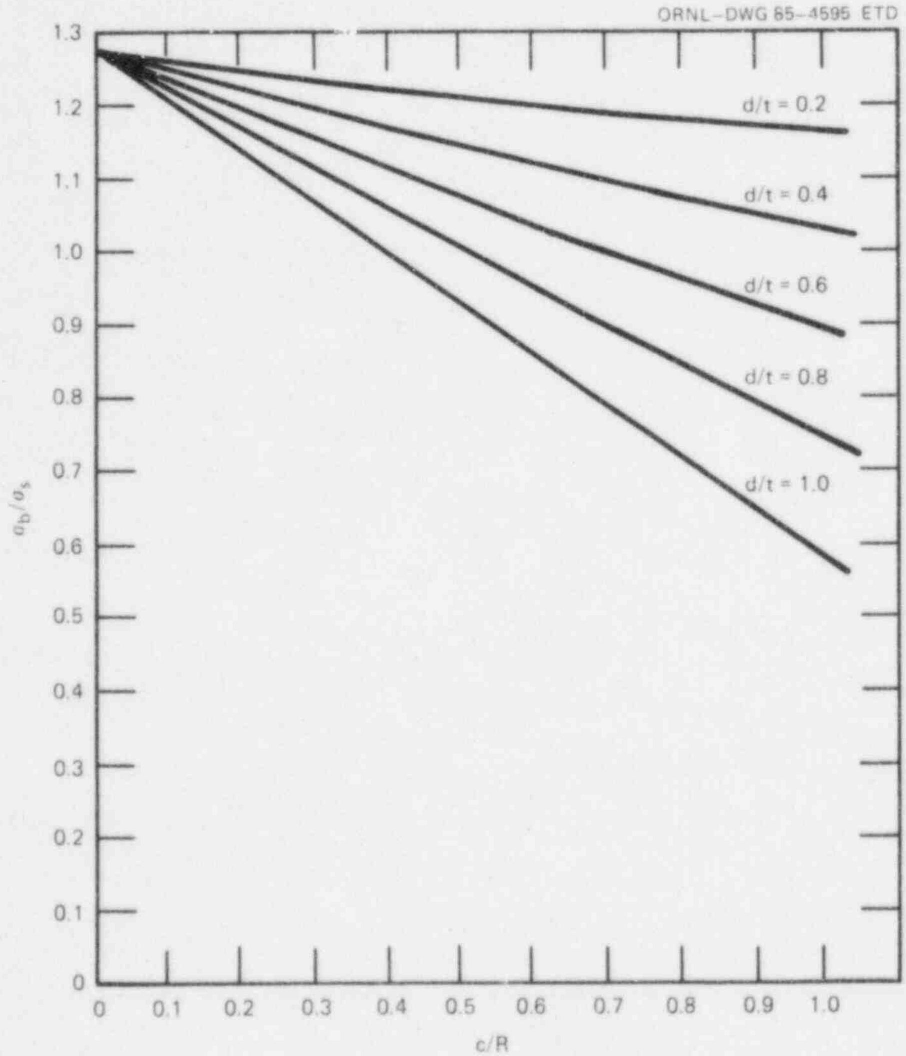

Fig. 7. Maximum load capacity of circumferential cracked straight pipe, bending moment loading [see Eqs. (7) and (8)].

However, there are a number of assumptions involved in the use of Fig. 8 and/or Tables IWB-3641-1 and -2 of the *Code*.[13] These are discussed in the following paragraphs.

1. The crack is in straight pipe. Figure 9 shows a girth butt weld between straight pipe end and elbow. When this elbow-pipe assembly is subjected to bending moments and the elbow parameter tR_b/R^2 is small, the axial membrane stresses at the girth butt weld are entirely different than in a pipe-to-pipe girth butt weld. While Fig. 8 is reasonably well confirmed for cracks in straight pipe, apparently no tests have been run of cracks at a pipe-to-elbow girth butt weld.

BWR primary coolant piping is generally Sched. 80 or heavier pipe. The elbow parameter for Sched. 80 pipe is not as small as, for example, in Sched. 10 or 40 pipe. Accordingly, the elbow-to-pipe girth butt weld, with adjacent cracks, response to pressure and moment loadings is probably not much different than the response indicated by Fig. 8.

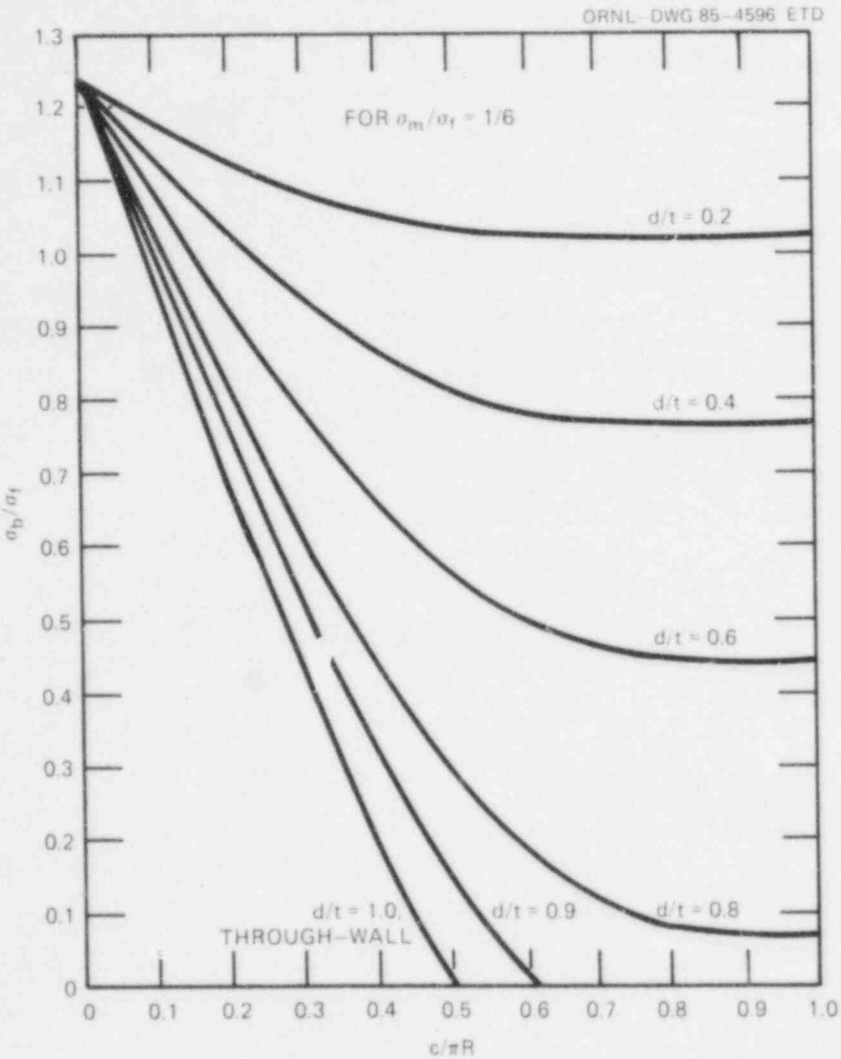

Fig. 8. Maximum load capacity of circumferential cracked straight pipe, axial and bending moment loading [see Eqs. (7–10)].

Some IGSCC indications have been found at Sweepolet-to-pipe welds; such cracks (if they exist) would not be covered by Fig. 8 and probably not by Fig. 1 either.

2. The wall thickness in the cracked region is constant. Detail A1 of Fig. 9 shows a girth butt weld cross section where the wall thickness t is constant and for which Fig. 8 is applicable. Detail A2 of Fig. 9 shows a girth butt weld in which the minimum thickness t_m is significantly less than the nominal wall thickness t. The problem is that there are two minimum wall thicknesses associated with piping design. One of the two minimums is that calculated as required for internal pressure by *ASME Code* Sect. III,[18] NB/NC/ND-3641:

$$t_{mr} = PD_o/[2(S + Py)] + A \ , \tag{12}$$

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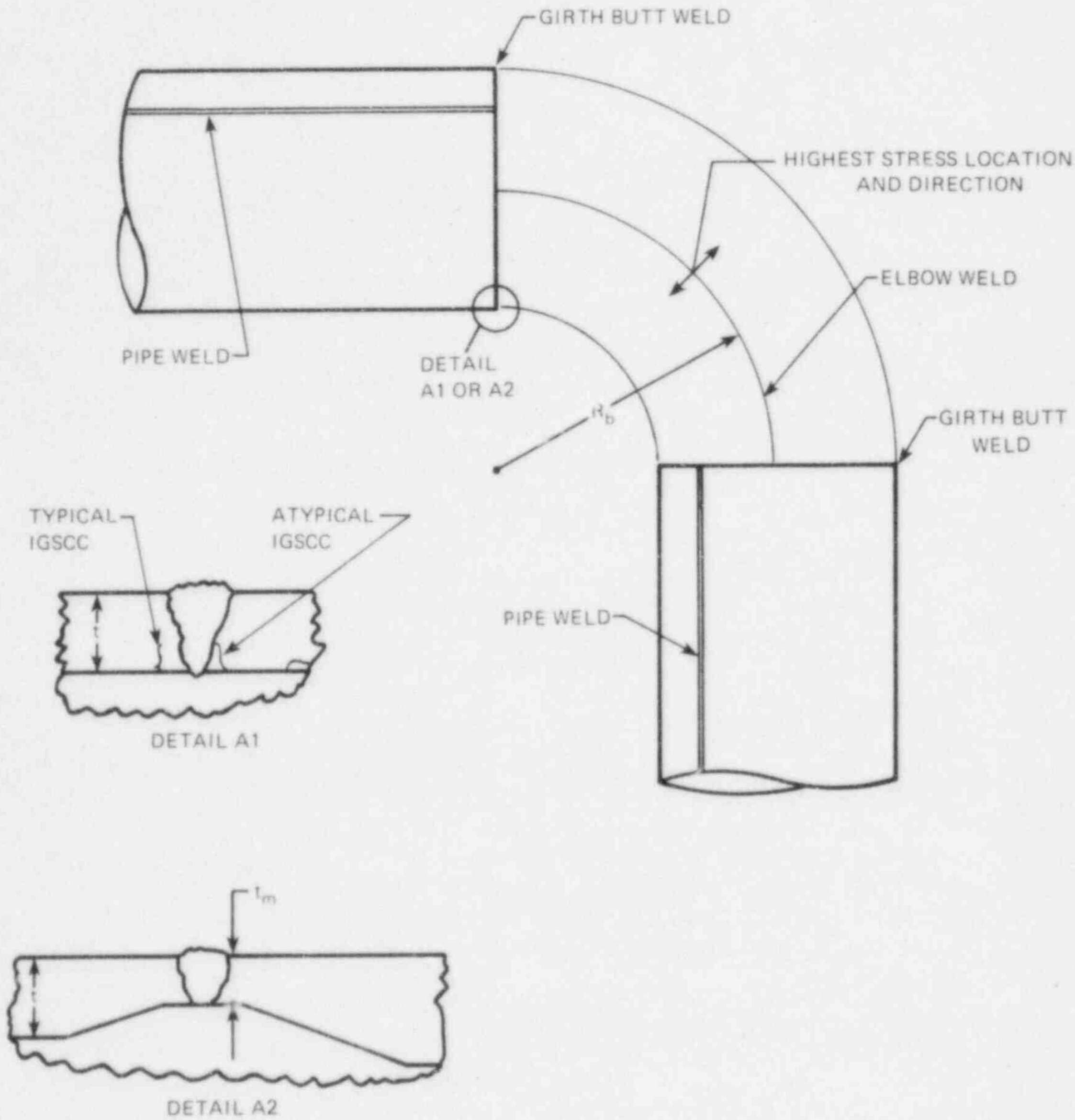

Fig. 9. Welds in piping components. Detail A1: Idealized girth butt weld constant around circumference. Detail A2: Not so idealized girth butt weld. Note that t_m can be significantly less than t. This kind of detail would probably vary around the circumference.

where

t_{mr} = minimum required wall thickness,
P = internal design pressure,
D_o = outside diameter of pipe,
S = *ASME Code* allowable stress intensity or allowable stress,
y = 0.4,
A = corrosion/erosion allowance.

The other of the two minimums is contained in piping material specifications. For seamless pipe:

$$t_{ms} = (7/8)\ t_{nom}\ , \tag{13}$$

or, for pipe made from rolled-and-welded plate:

$$t_{ms} = t_{nom} - 0.010\ \text{in.}\ , \tag{14}$$

where t_{ms} = material specification minimum wall thickness and t_{nom} = nominal wall thickness specified by material purchaser. The minimums given by Eqs. (13) and (14) are sufficient to ensure that the weldment region geometry is reasonably like Detail A1 of Fig. 9; hence, Fig. 8 would be applicable. However, the minimum given by Eq. (12) might give a weldment region geometry like Detail A2 in Fig. 9, for which Fig. 8 is not necessarily applicable.

In BWR piping, with indications found by UT, a weldment region like Detail A2 of Fig. 9 should be apparent from the UT inspection. If the value of t_m is clearly conveyed to the evaluator of the crack acceptability per IWB-3640, and if that evaluator uses t_m rather than t in his evaluations (in particular, in calculating applied stresses), Fig. 8 should be applicable with only minor reservations.

3. The material must be ductile. The tests that confirm the validity of Fig. 8 were mostly run on Type 304 or Type 316 stainless steel. However, welds in stainless steel pipe and cast stainless steel piping components (e.g., valves) may not be sufficiently ductile so that Fig. 8 is assuredly applicable to those materials.

In BWR piping, IGSCC has occurred in the base metal, slightly away from the weld, as illustrated in Fig. 9, Detail A1. Apparently, no IGSCC cracking has been found in pipe or elbow welds. Standard practice is to use annealed pipe and piping components; hence, those welds are not sensitized. However, as illustrated in Fig. 9, Detail A1, in at least one case, the IGSCC ran into a girth butt weld. Hence, the weld ductility is of some concern. Cast stainless steels and welds appear to be more resistant than wrought material to IGSCC. Accordingly, with a caveat on IGSCC that runs into weld material, Fig. 8 appears to be applicable to BWR piping with IGSCC.

4. A detectable leak will occur. Experimental and theoretical work[19] indicates that leak rates through IGSCC cracks should be sufficient so that the cracks will be found by leakage before unstable crack growth occurs. However, if there is anything that retards the leakage, then the leak-before-break defense may not exist. The Duane Arnold safe end cracking is an example of such a possibility. The cracking occurred under a thermal sleeve, and that sleeve, with accumulated crud between the sleeve and pipe, appears to have retarded the leak rate — fortunately, not to the extent that leakage was not detected. The experimental data on leak rates through cracks were necessarily of relatively short-time duration, and the question of crud blocking of an IGSCC crack itself does not seem to have been thoroughly answered.

Most welds in BWR piping do not involve thermal sleeves. Also, the seemingly favorable experience to date (no unstable crack growth incidents with perhaps around 100 leakers) suggests that crud blocking of an IGSCC crack is not probable.

5. Loads are not underestimated. Application of IWB-3640 assumes that results from the piping system analysis will provide an upper bound to bending moments and that maximum pressures are known. As discussed in Ref. 20, there are many uncertainties in piping system analyses. At present, loads on piping due to earthquakes are probably overestimated, but this may change in the future. However, the major problem area appears to be the potential for water hammer in piping systems. Water hammer produces both a short-time pressure increase and high bending moments. The three major breaks discussed previously in Sect. 4.1 involved water hammer. The prediction of the magnitude of water hammer is very difficult, and the prediction of the response of a piping system to a given magnitude of water hammer is also very difficult.

References 21 and 22 list numerous reported occurrences of water hammers in BWR piping system. Table 1-2 of Ref. 22 indicates 81 reported water hammer incidents through 1981. These have occurred in core spray, residual heat removal, and other systems, but apparently none nave been reported in recirculation piping systems, where most of the IGSCC has occurred. Accordingly, it appears that IWB-3640 can be used with a relatively high degree of confidence for the recirculation system of BWRs. For other BWR piping systems (e.g., residual heat removal), protection against water hammer consequences may be in the factors of safety used in IWB-3640; 3 for normal or upset, 1.5 for emergency or faulted. The simultaneous occurrence of a water hammer during an emergency or faulted event (e.g., a safe shutdown earthquake) is, in our view, quite low. Accordingly, the main question is whether the factor of safety of 3 is sufficient in BWR piping that may be subject to water hammer. Unfortunately, the question does not seem to be answerable. However, progress has been made in reducing the number of water hammers in BWR piping systems (e.g., 11 in 1975, 4 in 1981) and, with further progress in that respect, the probability of severe water hammers in BWR piping systems will be reduced to the extent that they will not contribute significantly to the failure probability of BWR piping with IGSCC. Nevertheless, at the present time, we view the water hammer potential as the major question concerning the assurance obtained by use of IWB-3640.

6. There are no significant design or fabrication errors. Of the three major breaks discussed in Sect. 4.1, one (Turkey Point 3) involved a design error and one (H. B. Robinson) involved a fabrication error. The DEGB that occurred in the German HDR in November 1983 (see Ref. 23) is a good example of a fabrication error; albeit in a decommissioned reactor being used for test purposes.

BWR piping systems with IGSCC are being extensively checked by UT. These examinations should detect any gross machining error such as occurred at the HDR. A careful piping system evaluation should uncover a design error such as the Turkey Point-3 relief valve header.

5.5 Tearing Instability Theory

The concept of tearing modulus T has been developed on the basis of the J-integral resistance curve and the nondimensional quantities T_m (materials tearing resistance) and T_a (applied tearing modulus). These quantities are defined:

$$T_m = (E/\sigma_f^2)\ (dJ_m/da) \ , \tag{15}$$

$$T_a = (E/\sigma_f^2)\ (dJ_a/da) \ , \tag{16}$$

where E = modulus of elasticity, σ_f = flow stress, and (dJ_i/da) = rate of change of J-integral as crack length a increases; i = m or a. The quantity (dJ_m/da) can be obtained from compact tension or three-point-bend material specimens as described in Ref. 24. It is a material property. The quantity (dJ_a/da) is calculated from the loads (axial forces, bending moments) acting on the cracked-pipe section and the through-wall crack length. The condition of stability of crack growth is given by:

$$T_m > T_a, \text{ stable crack growth} \ , \tag{17}$$

$$T_m < T_a, \text{ unstable crack growth} \ . \tag{18}$$

The tearing instability analysis is described and applied in Refs. 10, 25 and 26 and many other published reports and papers. It may be viewed as an alternative to the simple methods discussed previously in Sects. 5.1—5.4. Recent developments in the tearing instability theory appear to be directed towards circumferentially cracked pipe; we will confine our following comments to such cracks.

1. The tearing instability analyses have a subaspect in that if $J_a < J_{Ic}$, crack growth will not occur, where J_{Ic} is the material property that defines the initiation of crack growth.

From the limited comparisons we have made, it appears that, for through-wall cracks in wrought stainless steel such as Type 304, the tearing instability analysis gives close to the same results as the much simpler method represented by Fig. 8. However, according to Table F.6 in Ref. 10, for through-wall cracks in weld material normally used in joining Type 304 pipe material, the instability moment may be from 67 to 81% of that indicated by Fig. 8. Because IWB-3640 uses a factor of safety of 1.5 for emergency or faulted conditions, then the possible overestimate of moment capacity is about equal to the factor of safety. This is obviously a matter of concern with respect to IWB-3640 allowable crack sizes.

2. The tearing instability analysis provides an approach whereby displacement controlled loadings can be more accurately evaluated. The major example of such a loading is that due to restraint of free thermal expansion of the piping system. A cracked pipe acts like a local spring

in the piping system; as the crack grows, the bending moment decreases at the section containing the crack. To the extent that the σ_b in Fig. 8 or its equivalent P_b in IWB-3640 includes stresses due to restraint of thermal expansion, then there is a conservatism that would, in many piping systems, more than compensate for the weld material effect discussed in item 1 above. However, it is not clear in IWB-3640 that P_b includes restraint of thermal-expansion stresses. In our opinion, P_b in IWB-3640 *should* include restraint of thermal-expansion stresses.

3. While most of the tearing instability analysis reports and papers deal with through-wall cracks and circumferential crack growth, Refs. 10 and 27 apply the tearing instability approach to surface cracks where apparently radial crack growth through the wall precedes circumferential crack growth of the through-wall crack. The details of these analyses are not clear to us, but it appears that the tearing instability analysis is capable of identifying a boundary between stable and unstable surface cracks. Figures 4 and 5 illustrate efforts to establish this boundary by relatively simple methods. Figures 6–8, to the best of our understanding, identify stable/unstable regions based on load-controlled stresses. These figures indicate that failure of all surface cracks will be in the unstable region.

It appears that the tearing instability analysis has several advantages over the simpler methods discussed in Sects. 5.1–5.4, at the expense of considerable increase in the complexity of the evaluation. For example, to take advantage of the decreasing moment aspect in evaluating restraint of thermal expansion loads, an analysis of the piping system would be necessary, and that analysis would involve the many uncertainties discussed in Ref. 20.

Looking back at our discussion at the end of Sect. 5.4 and the six assumptions involved in the use of Fig. 8 and/or Tables IWB-3641-1 and -2, it appears that all of the assumptions except (3) are equally applicable to tearing instability analyses as developed to the present time. In view of items 1, 2, 4, 5, and 6 in Sect. 5.4, it would seem prudent to apply factors of safety on load to tearing instability analysis that are not less than the 3 (for normal, upset) or 1.5 (for emergency, faulted) used in IWB-3640. In view of additional uncertainties in the IWB-3640 basis, it would also seem prudent to increase those factors of safety a bit; for example, to 4 and 2 rather than 3 and 1.5, particularly where cracks in other than wrought stainless steel might be involved.

From NRC's standpoint of monitoring what is being done by licensees in evaluating cracks in safety-related piping systems, a simple method would have advantages. In this respect, the ongoing program at Battelle-Columbus Laboratories[28] has a main objective "to develop simple engineering analyses to assess the fracture behavior of nuclear piping." Such simple methods will probably have to be formulated to be adequately conservative with perhaps a penalty of being overconservative in some applications. The tearing instability analysis should prove to be a valuable optional method.

5.6 Probabilistic Evaluation of DEGB and Leak

Lawrence Livermore National Laboratory (LLNL) has completed evaluations of the probability of a DEGB in the primary coolant loops of Zion 1 (Ref. 29) and generic studies of the probability of DEGB in the primary coolant loops of Westinghouse[30] and Combustion Engineering[31] reactors. Probabilities of leaks were also calculated.

Volume 2 of Ref. 30 covers 17 Westinghouse plants located east of the Rockies. From Tables 5 and 6 of Vol. 2, average (of 17 plants) calculated leak and DEGB probabilities in the reactor coolant loop are as follows:

Leak: 3.6×10^{-6} during 40-year plant life,
DEGB: 1.6×10^{-10} during 40-year plant life.

The DEGB to leak ratio is then:

$$1.6 \times 10^{-10}/3.6 \times 10^{-6} = 0.000044 \ .$$

This ratio is about 3 orders of magnitude less than any of the break-to-leak ratios estimated in Sects. 3 and 4.

The LLNL approach is outlined in Fig. 10 (taken from Vol. 2, Ref. 30). The process is quite complex; a computer program, PRAISE, has been developed to carry out the detailed calculations (see Vol. 9 of Ref. 29). In the following, we touch on a few points to show how the LLNL approach relates to our discussions in Sect. 5.4 and to comment on the about 3 orders or magnitude lower break-to-leak ratio.

Figure 11 shows one primary coolant loop; there are 2, 3, or 4 such loops in each plant. The LLNL evaluation is restricted to postulated initial, semielliptical, circumferential cracks, as shown in Fig. 12, at the 16 girth butt welds indicated in Fig. 11.

The initial cracks that are postulated are a crucial part of what eventually is calculated for a leak or DEGB probability. Some relevant aspects of the initially postulated cracks follow:

1. The probability of a crack in any one of the welds is about 0.2.
2. The probability that the crack will have a depth $a > 0.25\ t$ is about 0.08.
3. The probability that the b/a-ratio (see Fig. 12) is >5 is about 0.01.

Having postulated initial cracks, a following step is to calculate how the cracks grow as a result of application of cyclic loadings during the 40 years of plant operation. The postulated loadings include the generally accepted transients for Westinghouse PWRs; for example, 200 cycles of heatup/cooldown, 18,300 cycles of unit loading/unloading of 5% of full power/minute, etc. (See Table 3 of Vol. 2, Ref. 30 for a complete list.) These transients have been used for many years as input to the *ASME Code* Sect. III[18] fatigue analyses and are generally deemed to be conservative. The fatigue crack growth uses reasonably well-established fracture-mechanics methods, two aspects of which are pertinent to this

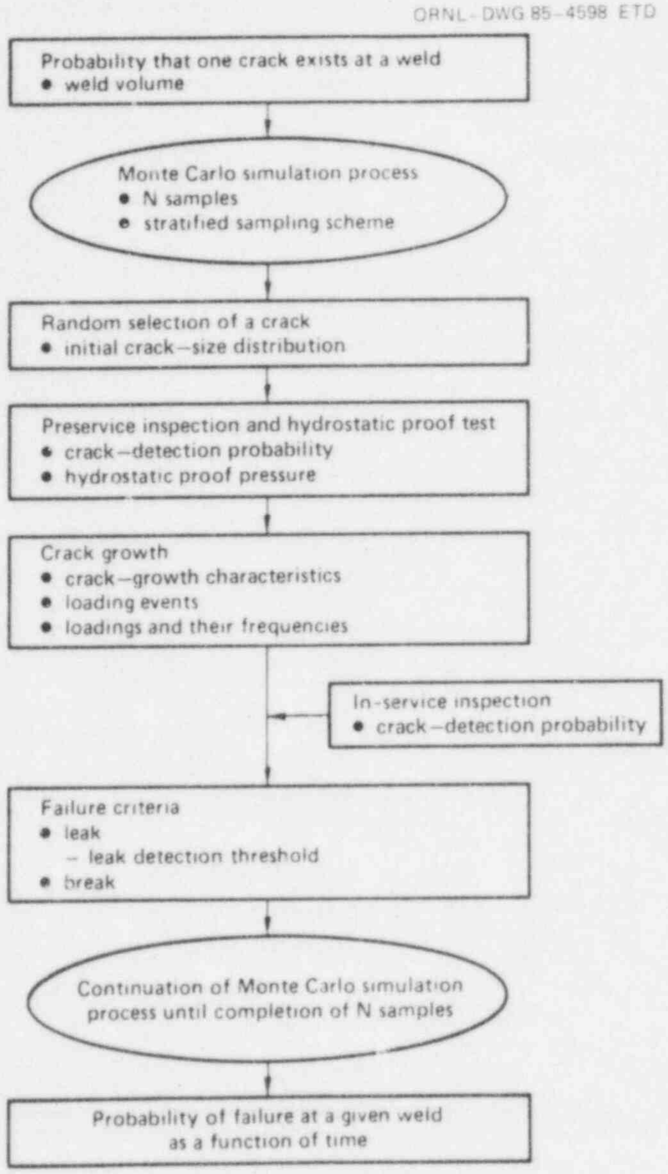

Fig. 10. Computational flow chart for estimating the failure probability of a given weld (from Ref. 30).

discussion.

1. Small cracks may not grow at all ($da/dn = 0$ if $\Delta K^* < 4.6$ ksi$\cdot\sqrt{in}$); other small cracks may grow only slightly so they do not lead to either a leak or a DEGB.
2. Cracks with large b/a-ratios tend to grow radially much more than circumferentially; that is, the crack grows through the wall without much increase in dimension b.

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*ΔK is the fracture-mechanics stress-intensity range.

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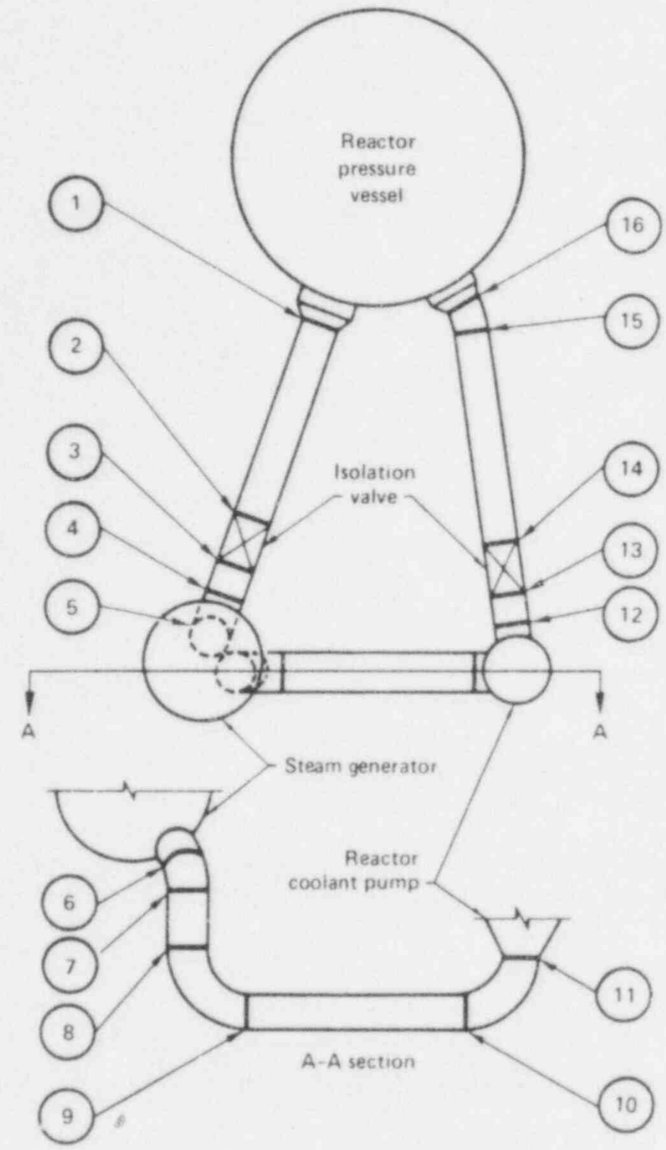

Fig. 11. Locations of 16 circumferential welds (per loop) evaluated in Ref. 30.

Having calculated crack growth, a following step is to estimate whether a leak or DEGB will occur. The failure criteria used appears to be essentially equivalent to that developed by Kanninen[17] and represented by Eqs. (7) and (8). Accordingly, Fig. 8 becomes relevant to the LLNL evaluation and provides a simple conceptual basis for showing that leak probabilities are very low and DEGB probabilities several orders of magnitude lower. To show this in a simple manner, we assume

1. cracks with $a/t < 0.25$ do not grow to either leak or DEGB,
2. cracks with large b/a grow through the wall without significant growth in the circumferential direction, and

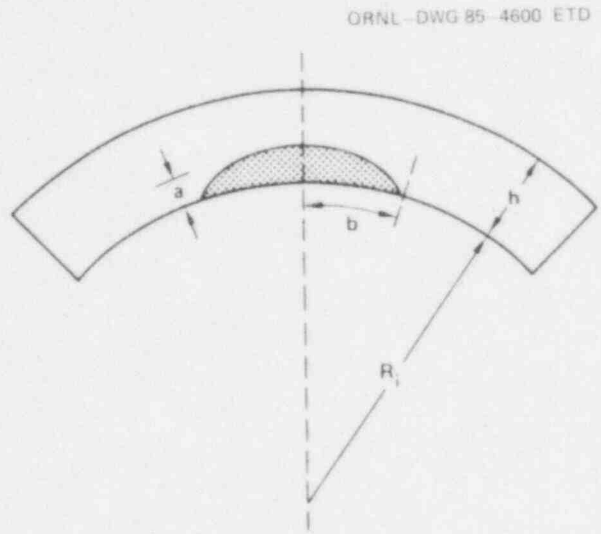

Fig. 12. Circumferentially oriented semielliptical pipe crack with depth a, half-length b, inside radius R_i, and wall thickness h (nomenclature used in Ref. 30).

3. in Fig. 8, $\sigma_m/\sigma_f = 1/6$ is representative of axial loads and $\sigma_b/\sigma_f = 0.3$ is representative of bending loads.

From Fig. 8, $c/\pi R \simeq 0.36$ at $\sigma_b/\sigma_f = 0.3$, $d/t = 1.0$. An equivalent b, considering the semielliptical shape when a has grown to t, is $\sim(\pi/4)c$. The primary coolant piping is such that $R \approx 14$ in. and $t \approx 2.5$ in.; hence $b = 0.36\ (\pi/4)(\pi/R) = 12.4$ in. With the assumption that cracks with $(a/t) < 0.25$ do not grow to either leak or break,

$$(b/a) = b(t/a)(1/t) = (14.5)(1/0.25)(1/2.5) = 19.9 \ .$$

Using the assumed initial crack distributions in Vol. 2 of Ref. 30, we can calculate the probability of the existence of a crack with $b/a > 19.9$, $a/t > 0.25$. The probability P_1 of such a crack in any of 64 welds in a 4-loop reactor is

$$P_1 = P_\beta(b/a > 19.9) \times P_a(a/t > 0.25) \times 64 \times 0.2 \ , \tag{19}$$

$$P_1 = 2.2 \times 10^{-7} \times 0.078 \times 64 \times 0.2 = 2.2 \times 10^{-7} \ .$$

The factor of 0.2 comes from the probability that a crack exists at all in any individual weld.

A similar crude approximation of DEGB probability P_d can be made by assuming that $c/\pi R = 0.7$ is representative of those cracks (with $a/t > 0.25$) that lead to DEGBs. Then the only factor that changes in Eq. (19)

is the P_β probability, which becomes

$$P_\beta\ (b/a > 23.2 \times 0.7/0.36) = 1.6 \times 10^{-11}\ .$$

The probability of DEGB is then

$$P_d = 2.2 \times 10^{-7} \times 1.6 \times 10^{-11}/2.2 \times 10^{-7} = 1.6 \times 10^{-11}\ .$$

Of course, the values of P_1 and P_d are very crude approximations, but hopefully, their calculation ties back to the discussion in Sect. 5.4 and illustrates how very low leak probabilities and several orders of magnitude lower DEGB probabilities can be calculated.

However, the main point of the preceding discussion was to indicate what is included in the LLNL evaluations. Specifically, an initial distribution of cracks was postulated to exist at the 16 girth butt welds in each loop as indicated in Fig. 11.

The following are not included in the LLNL evaluations: (1) cracks anywhere except at the girth butt welds; for example, no cracks in the base material or elbows even though the elbows may have longitudinal welds, and (2) no axial cracks anywhere. The primary coolant loops have a substantial number of branch connections and temperature detectors welded onto the main coolant piping. Industrial experience indicates that even a small crack at these locations could lead to a failure, albeit, very probably, not a DEGB.

The initial crack assumptions are deemed to be appropriate for LLNL's particular objective, that is, to examine the probability of a DEGB and the probability of a simultaneous occurrence of DEGB and an earthquake. However, there is a tendency to interpret these probabilities as being applicable to all possible failures in the primary coolant loops.

While fracture mechanics has made much progress in the evaluation of cracks in straight pipe, little is known about the behavior of cracks at branch connections subjected to pressure and moment loading. Accordingly, we can only speculate that the failure (as we have defined the term) probability is 2 or 3 orders of magnitude higher than the leak probabilities obtained in the LLNL probability studies. We would also speculate that the failures at branch connections, temperature detectors, elbow welds, etc., would *not* lead to DEGBs. These speculations then indicate that the break-to-leak ratio for primary coolant loops might be in the same ball park as the 0.03 estimated in Sect. 4, where, we emphasize, break is not necessarily and is unlikely to be a DEGB.

Insofar as we are aware, neither a leak nor a DEGB has occurred in the girth butt welds of the primary coolant loops of any PWR, domestic or foreign, during about 600 plant-years of operation. Using zero failures and assuming failures occur randomly in time leads to a 95% confidence upper bound on failure probability of about 0.005. Accordingly, the LLNL probabilities cannot be confirmed by experience to date.

6. DISCUSSION AND SUMMARY

In the Introduction, we postulated three NRC position changes that would seemingly follow if the leak-before-break concept were generally valid. We will frame our discussion summary around those three NRC position changes.

6.1 Elimination of Postulated Pipe Breaks

At present, one of the general design criteria [GDC-4, 10 *CFR* Part 50 (Ref. 32)] is that components important to safety shall be designed to accommodate loss-of-coolant accidents. Loss-of-coolant accidents means those postulated accidents that result from the loss of reactor coolant at a rate in excess of the capability of the reactor coolant makeup system from breaks in the reactor coolant boundary up to and including a break equivalent in size to the double-ended rupture of the largest pipe of the reactor coolant system.

Detailed guidance for implementation of postulated pipe breaks are contained in

Regulatory Guide 1.46, "Protection Against Pipe Whip Inside Containment" (Ref. 33).

Standard Review Plan 3.6.1, "Plant Design for Protection Against Postulated Piping Failures in Fluid Systems Outside Containment" (Ref. 34).

Standard Review Plan 3.6.2, "Determination of Break Locations and Dynamic Effects Associated with the Postulated Rupture of Piping" (covers both inside and outside containment) (Ref. 34).

6.1.1 High-energy piping systems

The detailed guidance for high-energy piping systems is abstracted in the following:

1. High-energy piping systems are defined as systems where the design temperature exceeds 200°F or the design pressure exceeds 275 psig.
2. Circumferential breaks (DEGBs) are to be postulated for pipe sizes >1-in. NPS. Longitudinal breaks are to be postulated for pipe sizes 4-in. NPS and larger. (Longitudinal breaks are parallel to the pipe axis at any location around the pipe circumference; the break area is equal to the cross section flow area upstream of the break location.)
3. Breaks are to be postulated in piping systems at:

   (a) terminal ends
   (b) intermediate locations where

   ASME Code[18] Class 1: $S_n > 2\ S_m$ for ferritic steel,
   $S_n > 2.4\ S_m$ for austenitic steel,
   $U > 0.1$,

where

S_n = primary-plus-secondary calculated stress, S_m = allowable stress intensity, and U = cumulative usage fatigue factor.

ASME Code[18] Class 2/3: $S_n > 0.8\ (S_h + S_A)$,
$P_e > 0.8\ S_A$,

where

S_n = calculated circumferential or axial stress, S_h = allowable stress at operating (hot) temperature, S_A = allowable expansion stress, and P_e = calculated restraint of thermal expansion stress.

(c) Intermediate locations in addition to those determined in (b) above, selected on a reasonable basis as necessary to provide protection. As a minimum, there should be two intermediate locations for each piping run or branch.

We comment, at this point, that:

1. The somewhat mysterious-seeming break location criteria in (b) were selected so that the *ASME Code* design procedures would be directly applicable to establishing break locations.
2. The postulated break requirements have a probabilistic flavoring; for example, high potential consequences only for high-energy fluids, DEGBs most likely at terminal ends or high stressed points.
3. Item (c) might be explained on the basis that knowledge of the failure locations is not very exact and, as a partial "fail anywhere" concept, it is desirable to have protection against at least two intermediate postulated breaks. This requirement is one of the nuclear industry's most painful aspects of the pipe break location guidance.

Protection against the effects of postulated pipe breaks can be obtained by separation of the piping from other essential systems, by enclosing or shielding either the piping or the other essential systems, or by installation of pipe whip restraints. Any of these protective measures can be very costly and lead to plant arrangements that can be less than optimum from an overall plant reliability standpoint.

The most significant postulated break is a DEGB in the primary coolant piping in PWRs of the vessel (see Fig. 11, locations 1 and 16). This postulated break may cause asymmetric loads on the reactor pressure vessel and its internals. At present, many newer plants incorporate massive pipe restraints to limit the pipe movement at these locations, as well as at other locations in the primary coolant loop of PWRs. It appears that the NRC's position is well on its way to eliminating postulated DEGBs for primary coolant loops of PWRs based on mechanistic analyses such as those given in Refs. 35 and 36 and probabilistic analyses, such as those done at LLNL and reported in Refs. 29–31. While the probability of break (50-gal/min leak rate) may not be as low as indicated by the LLNL work, we deem that the cost/benefits are such that postulation of DEGBs in PWR primary coolant loops should not be required by NRC.

The general question, of course, is to what extent breaks should be postulated for other piping systems. We have three questions with respect to the general question.

1. For reactor coolant pressure boundary leakage rates, there is a conceptual leak/break boundary in the normal makeup capacity, for example, 50 gal/min. What is a tolerable leakage rate in, for example, the service water system?

2. Leak detection systems exist inside containment. How does one ensure that leaks are detected outside containment such that a detectable leak will provide ample time to safely shut down and repair the leak?

3. Bolted-flanged joints are used in nuclear power plant piping systems. Also, such joints may be involved in valves and pumps. At present, the consequence of rupture of bolted-flanged joints can be considered to be enveloped by postulated DEGBs and/or large axial breaks in the piping. In the event that, on the basis of fracture mechanics, piping breaks are not postulated, what consideration is appropriate for bolted-flanged joints?

While these and perhaps many other questions will need to be addressed if postulated pipe breaks are to be generally eliminated, we deem that the state-of-the-art of fracture mechanics is such as to justify immediate consideration by NRC of licensee's requests to permanently remove (or not install) pipe restraints whose sole purpose is to protect against postulated pipe breaks.

It would seem desirable to start this process with high-energy piping inside containment on a case-by-case basis. NRC should provide acceptance criteria for eliminating postulated pipe breaks. It would seem desirable to have a simple acceptance criteria, along the lines of Sects. 5.1—5.4, with ample factors of safety to take care of the many uncertainties that still exist. The more rigorous method discussed in Sect. 5.5 should be permitted as an alternative approach where applicable (e.g., for pipe-to-pipe girth butt welds) with lower factors of safety.

Our acceptance of the state-of-the-art of fracture mechanics as sufficient to potentially eliminate postulated pipe breaks is not entirely motivated by the belief that we can accurately anticipate the conditions under which a pipe break will occur. Rather, we are aware of the extremely energetic and erratic nature of breaks in piping (or relatively small vessels) that are pressurized with steam or subcooled water. Accordingly, we have doubts as to whether pipe whip restraints will really restrain the pipe in accordance with the design intent. If not, then, of course, the benefits of the many expensive pipe whip restraints will be minimal.

6.1.2 Moderate-energy piping systems

Moderate-energy piping systems are those in which the design temperature is <200°F and design pressure is <275 psig. The service water system is an example of a safety-related moderate-energy system. NRC guidance consists of the postulation of through-wall cracks with an equivalent circular opening equal to that of one-half of the pipe diameter in length and one-half the wall thickness in width. The postulated crack is then assumed to spray on all components within the compartment.

Flooding effects should be based on a conservatively estimated time period required to effect corrective actions.

This guidance does not require pipe whip restraints and appears to be a less pressing problem than high-energy piping postulated pipe breaks.

6.2 Existence of Cracked Pipe and In-Service Inspection

While we view the state-of-the-art of fracture mechanics to be sufficiently advanced to be usable to eliminate some postulated pipe breaks, we do not consider it sufficient to justify indefinitely continued operation with a pipe that, on the basis of UT, is believed to contain cracks. In particular, we believe that the allowable crack sizes in IWB-3640 should be utilized only to justify continued operation over a limited period of time, for example, to the next refueling shutdown.

6.3 Summary

1. We deem the status of fracture mechanics to be sufficient to warrant consideration by NRC as a basis for elimination of postulated pipe breaks on a case-by-case basis.
2. We do *not* deem the status of fracture mechanics and the leak-before-break concept to be sufficiently general and proven, at this time, to justify indefinitely continued operation of safety-related, cracked piping.
3. We do *not* deem that the leak-before-break concept is anywhere near general enough or well enough proven to warrant any reduction in present requirements for in-service inspection of piping pressure boundaries.

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Leak-before-break entails the concept that, with a high degree of probability, failure of the pressure boundary of piping systems will be signaled by a detectable leak which will provide ample time to shut down and repair that leak. The status of the leak-before-break concept is discussed in this report, including a review of industrial and nuclear power plant experience with respect to leak-before-break, fracture mechanics and potential elimination of postualted pipe breaks in nuclear power plant piping design.

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