Reliability of Nondestructive Examination

Chapters 12 - 15

Prepared by S. H. Bush

Pacific Northwest Laboratory
Operated by
Battelle Memorial Institute

Prepared for
U.S. Nuclear Regulatory
Commission

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ABSTRACT

This eighteen-chapter, three-volume study evaluates the various nondestructive examination (NDE) techniques now used to detect flaws in components of nuclear systems so that the reliability of the techniques may be increased. The significance of flaws at various locations in pressure boundary components are assessed along with ways to optimize the NDE procedures needed to detect, locate and size them. Emphasis is placed on an integrated program which also considers design, fabrication procedures, and materials. The data available on the reliability of detecting, locating and sizing flaws by NDE are used to construct a probabilistic fracture mechanics model. The model highlights the significance of the failure to detect flaws, and to accurately locate or size them in the context of component failure probability.

This study was conducted under the U.S. Nuclear Regulatory Commission program on the "Integration of NDE Reliability and Fracture Mechanics." Its objectives include 1) improving examination procedures for incorporation into the American Society for Mechanical Engineers (ASME), Boiler and Pressure Vessel Codes, Section III, V, XI; and 2) gaining a better insight into the influence of improved reliability of NDE in detecting, locating and sizing flaws on component failure probabilities.

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ACRONYMS

ACRS	Advisory Committee on Reactor Safeguards
AD	Arbeitsgemeinschaft Druckbehälter
AD-RHP	Arbeitsgemeinschaft Druckbehälter-Reactor HP
AE	Acoustic Emission
AEC	Atomic Energy Commission
AEC/NRC	Atomic Energy Commission/Nuclear Regulatory Commission
AFML	Air Force Materials Lab
AH	Acoustic Holography
ALN	Adaptive Learning Network
ANL	Argonne National Laboratory
ARPA	Advanced Research Projects Agency
ASME	American Society for Mechanical Engineers
AVG	Abstand-Verstärker-Grosse
AVS	Distance Amplification Size
BNA	Barkhausen Noise Analysis
BWR	Boiling Water Reactor
CAP	Correct Acceptance Probability
CEGB	Central Electricity Generating Board
CRP	Correct Rejection Probability
DAC	Distance Amplitude Correction
DDP	Defect Detection Probability
DGS	Distance-Gain-Size
DG(z)	German Association for NDE Methods
DWR	Druckwasser Reaktor (German PWR)

EBS	Electronic Block Simulator
EDM	Electric Discharge Machining
EMA	Electromagnetic-Acoustic
EMAT	Electromagnetic Acoustic Transducers
EL	Error in Location
EPFM	Elastic-Plastic Fracture Mechanics
EPRI	Electric Power Research Institute
ES	Error in Sizing
ET	Engineering Testing
FRG	Federal Republic of Germany
GYFM	General Yield Fracture Mechanics
HAZ	Heat Affected Zone
HP	German Pressure Code Series
HSST	Heavy-Section Steel Technology
IAEA	International Atomic Energy Agency
ICP	Industry Cooperative Program
IGSCC	Intergranular Stress Corrosion Cracking
ISI	Inservice Inspection
ITV	Intermediate Test Vessel
IWG-RRPC	International Working Group on Reliability of Reactor Pressure Components
JAEC	Japan Atomic Energy Commission
LEFM	Linear Elastic Fracture Mechanics
LMFBR	Liquid Metal Fast Breeder Reactor
MPT	Magnetic Particle Testing
MMA	Manual Metal Arc (welding)

MIG	Metal Inert Gas (welding)
MITI	Ministry of Technology and Industry (Japan)
NASA	National Aeronautics and Space Administration
NBS	National Bureau of Standards
NDE	Nondestructive Examination
NRC-Res	Nuclear Regulatory Commission--Office of Research
PISC	Plate Inspection Steering Committee
PSI	Preservice Inspection
PT	Penetrant Testing
PVRC	Pressure Vessel Research Committee
PWHT	Post Weld Heat Treatments
PWR	Pressurized Water Reactor
PZT	Lead Zirconate Titanate (transducer)
QA	Quality of Acceptance
RF	Radio Frequency
RPV	Reactor Pressure Vessel
RSK	Reaktor-Sicherheit (German Reactor Safety Commission)
RT	Radiographic Testing
RTD-BAM	Röntgen-Technische-Dienst (RT)-Bundesanstalt für Materialprüfung (German Federal Institute for Material Testing)
SAFT-UT	Synthetic Aperture Focusing Technique for Ultrasonic Testing
SAR	Safety Analysis Report
SCC	Stress Corrosion Cracking
SET	Stress Exposure Technique
SMIRT	Structural Materials in Reactor Technology
SWP	Special Work Permit

USNRC	United States Nuclear Regulatory Commission
UT	Ultrasonic Testing
UT/ADP	Ultrasonic Automatic Data Processing System
WRC	Welding Research Council

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CHAPTER 12

PROBABILISTIC FRACTURE MECHANICS

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CHAPTER 12

PROBABILISTIC FRACTURE MECHANICS

12.1

INTRODUCTION

This chapter emphasizes the factors important in characterizing flaws for meaningful input into fracture mechanics equations. Obvious factors are flaw size and type, orientation and location. Information, when available on distribution of flaw sizes in a total flaw population, will be cited; however, emphasis will be on possible sources of error in presumed populations.

The current models for probabilistic fracture mechanics will be reviewed and presented to provide a basis for sensitivity studies using mechanical property data, load spectra and flaw-size distributions. The various mathematical functions used in these analyses are given in Chapter 11. Chapter 13 deals extensively with the reliability of flaw detection so such information will be somewhat limited in this chapter.

12.1.1 Deterministic Versus Probabilistic

Although the ASME Code deterministic approach establishes conservative load limits and property levels, factors outside the scope of both code and designer may lead to failures so that failure probability plays a role even in the so-called deterministic approach. The following paragraphs simplistically discuss the key parameters of loads, material properties, and flaw geometry as they relate to failure of structures.

Figure 12.1.1 uses a stress-strain diagram to portray load thresholds and failure mechanisms. Zone I failures occur when the ultimate strength is exceeded. In this mode, if one assumes that code maximum loads remain the same, failure will occur through a loss of section such as may occur with gross cracking. Zone II represents elastic-plastic or general yield failure where

FIGURE 12.1.1. Simplified Stress-Strain Diagram Illustrating Failure Regions for Net Section Failure (I); Elastic-Plastic or General Yield (II); and Linear-Elastic (III); Assumes Membrane (Tensile) Load

the flaw contributes to failure at stresses below the ultimate. Zone III is the region where linear elastic fracture mechanics controls and failure will occur without perceptible deformation.

The three parameters contributing to failure--namely, loads, mechanical properties and flaws--are tabulated in Tables 12.1.1(a), (b) and (c) to illustrate loads in and out of a design scope, the possible role of mechanical properties in failure, and a few comments on flaw geometries.

TABLE 12.1.1(a). First Parameter Contributing to Failures: Loads

Design

Normal

Membrane (Tensile) - bending, torsional; either static or vibrational

Upper Bound (Faulted/Low Probability)

Normal plus dynamic (Examples, water hammer, seismic, certain transients)

Not in Design Scope

External - High- or low-cycle thermal fatigue (often not factored into design)

Internal - Residual stresses rarely handled in design

TABLE 12.1.1(b). Second Parameter Contributing to Failures: Mechanical Properties

Net Section Yielding and Failure (Tensile Stress Controls)

Little or no notch sensitivity

General Yielding

Failures occurs well below σ_{UTS} in presence of flaw

General yield fracture mechanics

J_{IC} an indication of toughness at failure

Local Yielding in Vicinity of Flaws

Failure occurs below or slightly above yield point

Elastic-Plastic fracture mechanics

TABLE 12.1.1(b). (contd)

J_{IC} an indication of toughness at fracture (This J_{IC} is lower than J_{IC} for GYFM)

No Observable Deformation of Yielding

Failure may occur well below yield

Linear elastic fracture mechanics

K_{IC} is an indication of toughness at fracture (K_{IC} will be a function of section size and/or notch toughness properties)

Above limited to Mode I (Tensile) Behavior

TABLE 12.1.1(c). Third Parameter Contributing to Failures: Flaws

1. Fracture mechanics approaches assume a planar (two-dimensional) flaw, usually of a simple geometry (circular, semi-circular, elliptic, semi-elliptic)
2. Three-dimensional, or nonplanar two-dimensional flaws will exceed in critical size the planar flaw
3. Flaw location and flaw orientation will increase or decrease the flaw size for failure usually due to the increase or decrease in effective stress on the flaw

In Tables 12.1.2(a), (b), and (c), an attempt is made to illustrate failure modes where two of the three parameters remain fixed and the third varies. (Cases of actual failure are cited to indicate that they are not hypothetical.) For example, Table 12.1.2(a) holds the flaw size constant and design loads are not exceeded. Table 12.1.2(b) permits design and nondesign loads to vary, and Table 12.1.2(c) considers initiation and growth of flaws with loads and material properties remaining within defined limits.

An assessment of these tables confirms the original thesis that factors outside the scope of design rules or codes such as ASME can lead to failure. Also, probabilities of failure may vary widely even when a system operates within the bounds of load and material properties set by the code.

The deterministic approach to such behavior as component failure has proven satisfactory and relatively straight-forward, given that the relevant

TABLE 12.1.2(a). Condition A - Flaw Dimensions Fixed; Design Operating Loads Not Exceeded

Material Factors Leading to Failure

Case I

$K_{IC} < K_I$, based on loads and flaw dimensions; component fails, probably catastrophically; examples:

1. Liberty Ship hull failures.
2. Large steam turbo-generator rotor failures.

Case II

$K_{IC} > K_I$ initially; however, time-dependent embrittling mechanisms lead to $K_{IC} < K_I$; component fails, probably catastrophically; examples:

1. Hydrogen embrittlement - pipe and vessel failures in "sour" crudes; ferritic or austenitic alloys.
2. Temper embrittlement - turbine rotor failures; ferritic alloys.
3. Sigma or Chi-phase formation - failures of duplex ferrite-austenite alloys due to long-term embrittlement; cast valve bodies.

Case III

K_{IC} varies within a component; If flaw is adjacent to a low K_{IC} value region, it may "pop-in" under the design load, and the larger flaw may continue to run into and through tougher regions; examples:

1. Heavily banded ferritics - typical of acid-open hearth steels where segregation (P, Cu, etc.) leads to regions of low fracture toughness.
2. Turbine rotor failure.

Case IV

K_{IC} varies due to fabrication process or to operation; If flaw is located in region of low K_{IC} , it may "pop in" and run; examples:

TABLE 12.1.2(a). (contd)

1. Grinding or cold working may degrade surface K_{IC} properties - Such working may lead to high surface tensile residual stress. The combination of loads has led to failure in turbine rotors.
2. Irradiation embrittlement - It can lead to a damage gradient through a thick section; conceivably, a pre-existing flaw could propagate rapidly under some combination of load, and may or may not arrest. No known cases.

TABLE 12.1.2(b). Condition B - Flaw Dimensions Fixed:
Material Properties Fixed

Load Factors Leading to Failure

Case I

K_{IC} (or J_{IC}) $\gg K_I$ or J_I for operating loads; faulted loads increase K_I (J_I) so K_I (J_I) $> K_{IC}$ (J_{IC}) component fails; examples:

1. Turbine overspeed leading to disc or rotor failure or both.
2. Water hammer or water slugging causes piping failure.

Case II

Local stress field from residual stresses complements design loads. (An embedded flaw could propagate to and around surface.) Change in flaw size and length relative to depth could cause failure under design loads.

TABLE 12.1.2(c). Condition C - Either No Flaw or Subcritical Flaw;
No "Faulted" Loads; Material Properties Assumed
Unaffected with Time

Case IA

No pre-existing flaw; static loads only; crack should not initiate.

Case IB

No pre-existing flaw; design static and cyclic loads; crack should not initiate.

TABLE 12.1.2(c). (contd)

Case IC

No pre-existing flaw; static loads only; aqueous environment; crack may initiate by stress corrosion; presence of residual stresses at surface increases probability of crack initiation and propagation. Many cases of SCC in austenitic and ferritic components; if no pre-existing flaw; cyclic and static loads; aqueous environment; pitting corrosion may occur and pit serves as crack initiator for corrosion-fatigue mechanism.

Example: Turbine rotors have cracked nearly through. (Critical pit size for crack initiation ~0.25 mm.)

Case II

No flaw in usual sense; grinding marks, scratches, nonmetallic inclusions; cyclic loads. (Above defects serve as crack initiators in same sense that corrosion pit does.) Difference in crack growth rate possible due to presence or absence of environment; for example, rate in air may be one-tenth to one-hundredth of that in water. [Contaminants (NaOH, NaCl, etc.) in water may increase rates by factors of ten to thousand.]

Case III

Pre-existing flaw; combination of cyclic and static loads, crack may grow; rate dependent on flaw location and types of load. Surface cracks and bending loads lead to higher rates than embedded flaws. Many cases of fatigue under these conditions.

Case IV

No flaw in usual sense; may be grinding marks, scratches, etc.; usual design loads; either high-cycle thermal fatigue, low-cycle thermal fatigue, or a combination of both due to thermal gradients; crack should initiate and may or may not grow; examples:

1. BWR nozzles - high-cycle thermal fatigue initiates cracks in cladding but not enough driving force to propagate; if low-cycle fatigue component present, crack will propagate.
2. PWR feedwater line; combination of high- and low-cycle thermal fatigue results in both initiation and propagation of cracks.

DETERMINISTIC ENGINEERING ANALYSIS

"TYPICAL" ANALYSIS INDICATES ADEQUATE SAFETY MARGIN

"WORST-CASE" ANALYSIS INDICATES NEGATIVE SAFETY MARGIN OR FAILURE

t = "TYPICAL"

w = "WORST-CASE"

FIGURE 12.1.2. Contrast Between Deterministic and Probabilistic Engineering Analysis. Failure possible over the crosshatched region.

data base is sufficiently large to permit meaningful predictions. Generally, with a large data base, a direct relationship can be established with actual field experience. Unfortunately, the deterministic approach becomes less effective as the data base decreases, or when one wishes to extrapolate beyond the data base. For example, predicting failure behavior at higher stresses, pros and cons of modified nondestructive examination techniques, behavior near end of design life, etc.

The probabilistic approach involves the development and application of engineering models based on an understanding of failure modes and on the statistical distribution of the various controlling parameters. Basically, one analyzes failure by interrogation from the inside rather than from the outside of the system. Even so one starts with a deterministic model of one or more system failure modes, then modifies them appropriately to the probability format. Such physical models include fracture mechanics, fatigue life analyses, static stress strength analyses, etc. Probability is applied by combining known, assumed or proposed statistical variations of the various controlling parameters to predict the statistical variations in structural performance.

Figure 12.1.2 compares the two approaches.^(12.1.1) The deterministic approach utilizes fixed values such as typical or worst-case to establish the adequacy of safety margins. The probabilistic approach utilizes probability density functions (PDF) for significant parameters. In this comparison, variations in strength and applied stress were considered. The region of concern will be the crosshatched area of overlap where stress levels exceed strength levels.

The following sections will deal with the specific case of piping in the context of flaw-size distribution, loads, and mechanical properties. Much of the information was derived from work on probabilistic modeling of pressure vessel failure. Figure 12.1.3 from Becher and Pederson^(12.1.2) or Becher and Hansen^(12.1.3) is an excellent presentation of the parameters affecting probability of failure. Initial flaw distribution, transients affecting flaw growth, loads, stress intensities, and fracture toughness are tied together to yield failure probability.

FIGURE 12.1.3. General Procedure for Calculating Time-Dependent Failure Probability

One of the most critical input parameters, and the one we know the least about, is the flaw-size distribution probability density function at any time (T). Figure 12.2.1 is an attempt to represent schematically the time-dependent effects relevant to piping fracture. The use of a population of cracks initiated and grown exclusively during the operating life T_{op} is justified on the basis of observed plant behavior.

FIGURE 12.2.1. Schematic Diagram of Time-Dependent Effects Relevant to Piping Fracture

Table 12.2.1 from reference 12.2.1 gives some idea of the sensitivity of pipe size and fabrication conditions to failure rate in the case of IGSCC in BWR primary coolant systems. Such defects in almost all cases are operationally initiated requiring no prior fabrication crack.

TABLE 12.2.1. Failure Rates Due to IGSCC in Weld Heat-Affected Zones of BWRs (Multiply all values by 10^{-4})

Failures	Dresden 1		BWR Mark 1 and 2		BWR Mark 3 and 4	
	Per Weld	Per Weld-Year	Per Weld	Per Weld-Year	Per Weld	Per Weld-Year
All welds	395	25.1	22.0	2.59	60.3	15.1
2-in. (51-mm) line welds			30.5	3.59		
4-in. (102-mm) line welds	845	53.9	50.5	5.94	251.6	62.9
6-in. (152-mm) line welds	1684	106.8	40.7	4.79		
8-in. (204-mm) line welds	909	57.7	40.2	4.73	141	35.3
10-in. (254-mm) line welds					125	31.2

The statement of the flaw-size distribution problem is relatively straight forward; unfortunately, this is not true with the answer. Basically, the problem is—how well can we define the shape and dimensions of the probability density function for the flaw-size distribution at any time (T)? The answer depends on the following factors at a minimum:

- original flaw-size distribution
- accuracy of the fatigue crack growth equation used
- correct definition and applications of all transients causing crack growth
- effects leading to the initiation and propagation of new cracks by mechanisms such as IGSCC, corrosion-fatigue, thermal fatigue, etc. (Such effects may include but not be limited to the environment seen by the system, material properties, type of alloy, degree of sensitization, condition of surface, level of residual stress, existence of crevices, etc.)

- adequacy of NDE techniques to detect both fabrication and operationally initiated flaws. (This is better defined as the probability of detection as a function of flaw size, location and orientation.)
- degree of corrective action taken to repair defects
- time since last NDE permitting further growth of existing flaws or initiation and growth of new flaws.

Presumably, if one can correctly define all of the above, one can define the flaw-size distribution at time (T).

12.2.1 Known Flaw-Size Distributions

Actual data on flaw-size populations in either reactor pressure vessels or in piping are limited. Examples of sources of this data and the kinds available are given in the following:

- In the Clinch River Breeder Reactor, an exponential distribution of flaw sizes was assumed.
- Wilson^(12.2.2) provided conditional probabilities of flaw size for a spectrum of pipe sizes with the data fitting a log-normal distribution. Both the selection and distribution was arbitrary and not based on data.
- Lidiard^(12.2.3) in the Marshall Report^(12.2.4) reports on the use of a limited population of flaws in reactor pressure vessels which probably is not too relevant to piping. These followed an inverse exponential distribution. Harrop^(12.2.8) modified these models.
- Harris^(12.2.5,12.2.6) used several sources with emphasis on Wilson.^(12.2.2)
- Becher and Hansen^(12.1.3) in their reports present actual flaw size distribution data. They use radiography to size the length of the flaws in pressure vessels, and the flaw-size distribution obeyed a log-normal relationship. To obtain the critical parameter, flaw depth, some relationship between length and depth must be assumed.

- Cramond^(12.2.7) reported studies in Japan on ship welds, in Czechoslovakia on a reactor pressure vessel weld, and in the USSR in carbon steel butt welds. These will be discussed further.
- The work of Salter and Gethen^(12.2.9) also cited in Chapter 10^(10.5.1) reported flaw-size distributions in pressure vessel weldments. These are discussed further.
- Jouris and Shaffer^(12.2.10) report results of studies at Svejsecen-tralen (Danish Welding Institute). While flaw sizes distributions are generated, there is little or no data relevant to either welding processes or materials.
- Forli et al.^(12.2.11) reported on a cooperative Scandinavian effort comparing flaw-size distributions as detected by both RT and UT. Flaw sizes were available for cases of incomplete penetration and are discussed further. In addition, inferred distributions exist for slag inclusions, and lack-of-fusion. Forli^(12.2.12) reported additional data generated by the joint Scandinavian effort on NDE reliability. Specific information, probably generated by Svejsecen-tralen, is discussed further.
- Hansen^(12.2.13) reported on a variety of defects in weldments; however, the data are not in the public domain and will not be reviewed further.
- Raussi and Tiainen^(12.2.14) developed data on defect-size distri-butions in ferritic piping weldments. These appear later.
- Trumpfheller^(12.2.15) presented extensive data on defect-size dis-tributions in weldments typical of pressure vessels. These are pre-sented later.

Figure 12.2.2 from reference 12.2.4 represents an initial and very tenta-tive distribution of flaw sizes relevant to fairly thick sections typical of reactor pressure vessels. It is used to illustrate a trend and is not consid-ered typical of piping. Hopefully, work now underway in the U.K. will provide added information on flaw-size distributions.

FIGURE 12.2.2. Diagram Showing a Plot of $\log_{10} A(x)$ (Number of Flaws) Versus Crack Depth

At best, we can assume the flaw-size distribution approximates the shape in Figure 12.1.3.

The three studies reported by Cramond^(12.2.7) are indirectly relevant to ferritic piping insofar as thickness and welding techniques are concerned. Tables 12.2.2, 12.2.3 and 12.2.4 present the data. The bases for the data in Table 12.2.2 were examination by radiography of 7073 feet of manual metal-arc butt welds and 4061 feet of automatic submerged-arc welds from ships under construction. Plates ranged from 15- to 35-mm thick.

TABLE 12.2.2. Estimated Major Flaw Dimensions Based on Japanese Ship Data(12.2.7)

Flaw Type	Welding Method	Observed Number	Spatial Density Flaws/ 10^3 in.	Mean Length (in.)	Coefficient of Variation
Pipe	Automatic	215	4.4	0.322	0.46
	Manual	45	0.41	0.331	0.42
Slag	Automatic	251	5.1	0.255	0.54
	Manual	2569	32.7	0.261	0.46
Lack of penetration	Automatic	234	4.7	0.462	0.18
	Manual	336	2.8	0.430	0.22
Lack of Fusion	Automatic	5	0.10	--	--
	Manual	259	3.1	0.390	0.30
Cracks	Automatic	15	0.31	0.289	0.41
	Manual	18	0.20	0.472	0.08

A Czechoslovakian study used UT, RT, MT of electroslog and automatic and manual arc welds. The data are given in Table 12.2.3. A majority of the defects were slag inclusions.

The third study presented data for defects found in automatic submerged-arc butt welds in carbon steel. Some 5700 x-ray films were examined with presumed sensitivities of 1.5 - 2.0%.

Equations describing the flaw-size distribution have been reported^(12.1.2,12.2.3); however, both sets of equations require clarification concerning the bases for establishing the values of the constants cited. Becher and Pederson^(12.1.2) suggest an equation of the following form for the crack-size probability density function (PDF):

$$A(X) = A \exp [-\lambda(X-X_0)] \quad (12.2.1(a))$$

where X is crack depth with constants yielding,

$$A(X) = 2.56 \exp [-2.56 (X-0.1)] \quad (12.2.1(b))$$

TABLE 12.2.3. Summary of Flaws in Welds(12.2.7)

A. Multilayer Circumferential Arc Welds

Location	B Thickness (in.)	Length (in.)	Spatial Flaw Density (Flaws/10 ³ in.)			Estimated Mean d (in.)
			d=0.098 -0.138	d>0.138	All (All,B-1")	
Welds on Bottom Sect.	7.87	1228	87.98	6.51	94.44 (12.00)	0.121
Welds on Top Section	5.91	2551	7.84	0.0	7.84 (1.32)	0.118
Field Welds	5.91	1913	13.59	1.57	15.16 (2.65)	0.122

B. Electroslag Welds (Ultrasonic Inspection)

Component	Thickness (in.)	Length (in.)	Spatial Flaw Density (Flaws/10 ³ in.)			Estimated Mean d (in.)
			d=0.059 -0.079	d=0.079 -0.138	d>0.138 All (All,B-1")	
Support Shell	7.09	775.6	0.0	2.58	1.29 3.87 (0.54)	0.128
Smooth Rings	7.09	2563.0	2.73	0.78	0.78 4.29 (0.61)	0.092
Nozzle	8.27	2834.6	0.35	0.70	0.0 1.06 (0.12)	0.095
Bottom Cap	9.84	669.3	2.99	2.99	0.0 5.98 (0.61)	0.089
Cover Cap	12.99	1960.6	3.06	1.53	1.02 5.61 (0.43)	0.098
Flange Ring	17.32	657.5	0.0	0.0	0.0	--
Bottom and Cover Rings	24.41	763.8	37.97	18.33	0.0 56.3 (2.30)	0.082

TABLE 12.2.4. Relative Frequency of Defects in Automatic Sub-Arc Welds in Carbon Steel(12.2.7)

Plate Thickness (cm)	Frequency of Defects (%)			Cracks(a)
	Porosity	Slag Inclusion	Lack of Fusion(a)	
10	50	40	10	0
12	62	28	8	2
16	70	26	4	0
22	64	34	2	0
26-28	62	28	10	0

(a) Difficult to detect; sensitivity 1.5-2.0%.

Purportedly, the above probability density function was derived from values suggested by O'Neil and Jordan in the 1972 Conference on Periodic Inspection of Pressure Vessels. The basis of the PDF is unclear. Terminology in equations has been modified to be consistent with other references in this chapter.

Lidiard and Williams^(12.2.3) cite a similar equation for the flaw-size distribution as well as the detection probability. Here too, the values of the constants appear to be quite arbitrary, based on answers to a somewhat ambiguous questionnaire. Their equation for the crack-size PDF was

$$A(X) = A \exp(-\lambda X) \quad (12.2.2(a))$$

$$A(X) = 14.8 \exp(-4.06X) \quad (12.2.2(b))$$

where X is the flaw depth. Units were in reciprocal inches for both equations. The general equation incorporating detection is reported in the next section.

Harrop^(12.2.8) discussed an expansion of the model developed by Lidiard and Williams^(12.2.3) and applicable primarily to the reactor pressure vessel. The model assumes each NDE is independent of all others, each examination is equally efficient regardless of time and location, and detected cracks are assumed to be removed with a perfect repair. The crack-size distribution after P independent examinations is denoted by $N_{tp}^{(P)}(X)$; so after the examination

$$N_{t_p}^{(P)}(X) = N_{t_p}^{(P-1)}(X) B(X) \quad (12.2.3)$$

where $B(X)$ is the probability of not detecting the flaw.

Harris^(12.2.6) utilized error functions to evaluate the flaw distribution. A cumulative density function (CDF) fitting a log-normal relationship conditional crack distribution was used:

$$P_{\text{cond}}(> a_1) = \frac{N(> a)}{N_0} = \frac{1}{2} \operatorname{erfc} \left(\frac{1}{\mu^{1/2}} \ln a_1/\lambda \right) \quad (12.2.4)$$

Values of the parameters μ and λ will depend on the specific flaw model used.

The probability density function (PDF) obtained by differentiating the preceding equations^(12.2.4) will be

$$p_0(a) = \frac{1}{\mu a (2\pi)^{1/2}} \exp - \frac{1}{2\mu^2} (\ln a/\lambda)^2 \quad (12.2.5)$$

where $p_0(a) da$ will be the portion of cracks defined as significant that fall in the range a to $a + da$. This crack population is given by

$$\frac{n(a) da}{n_v(l_0)} = p(a) da \quad (12.2.6(a))$$

for a unit volume. For a general volume this becomes

$$\frac{n(a) da}{N} = \frac{n(a) da}{V n_v(l_0)} = p(a) da \quad (12.2.6(b))$$

The real concern is the probability that a crack of a size greater than a exists in the system under study.

Harris^(12.2.6) denotes this probability as p^* so we can have

$$P(>a) = P_{\text{cond}}(>a)p^* = \frac{1}{2} p^* \operatorname{erfc}\left(\frac{1}{\mu^{1/2}} \ln a/\lambda\right) \quad (12.2.7(a))$$

or

$$\frac{P(>a)}{p^*} = \frac{1}{2} \operatorname{erfc}\left(\frac{1}{\mu^{1/2}} \ln a/\lambda\right) \quad (12.2.7(b))$$

where p^* is denoted by

$$p^* = 1 - (1 - p_V^*)^V \quad (12.2.8)$$

where p_V^* is probability of having a crack of concern in a unit volume. p_V^* usually will require experimental data to define values.

The parameters μ and λ as well as others cited in subsequent sections of the report can be considered as distributed functions. Specifically, μ is considered normal and λ log-normal. For Harris' case $P(>a)/p^*$ versus a yields a family of curves such as given in Figure 12.2.3 using Wilson's^(12.2.2) information.

12.2.2 Defect-Size Distributions

Defect-size distributions in both pressure vessel and piping weldments have become increasingly important in the assessment of component reliability. The following figures represent an updating of sources as of early 1982. They are presented for information only, and no claims are made as to their validity. An effort has been made to permit the reader to assess whether the assessment was based on large or small numbers, and permit some references as to applicability.

Figures 12.2.4 and 12.2.5 from Salter and Gethin^(12.2.9) cover defect-size distributions in ferritic steels typical of some pressure vessels ranging in thickness from 12 to 125 mm (~0.5 to 5 in.). Defects included cracks,

FIGURE 12.2.3. Various Percentiles of an As-Fabricated Crack-Size Distribution ($P(>a/p^*)$) as Calculated by Monte Carlo Using Distributed Values of μ and λ

lack-of-fusion, porosity and inclusions. Figure 12.2.5 permits a comparison of the role of the welding process. The same defect size data are presented in terms of type of weldment, either circumferential or longitudinal, and welding operation, either submerged-arc, manual metal arc or electro-slag.

Jouris and Shaffer^(12.2.10) used data developed by Svejsecentralen to generate equations applicable to initial flaw-size distribution. Although the data suffer from a lack of details, they are reported in Figure 12.2.6.

Forli et al.^(12.2.11) reported on defect-size distributions for several types of defects. Incomplete penetrations in weldments are reported here because they were sized. Again, not too much was reported concerning the conditions of base metal, welding conditions or weldment. Figure 12.2.7 contains these data.

FIGURE 12.2.4. Analysis of Types of Weld Defects in Terms of Numbers and Length for Various Thickness of Ferritic Steel Plate

(a) Expressed as $\frac{\text{Total Defect Length}}{\text{Total Seam Length}} \times 100$.

(b) Total Length of Weldment, Meters, Cracks and L-O-P Considered Critical.

Forli^(12.2.12) in a report of the work covered in reference 12.2.11, described four classes of defects -- lack-of-penetration, lack-of-fusion, a combination of L-O-P and L-O-F, and porosity. These were reported in terms of defect height and were derived from ~35 m of mild steel weldment covering the range of thickness 10 to 26 mm (≤ 1 in.). Figure 12.2.8 presents these data.

FIGURE 12.2.5. Analysis of Weld Defects by Type of Joint and Welding Process

- (a) Expressed as $\frac{\text{Total Defect Length}}{\text{Total Seam Length}} \times 100$.
 (b) Total Length of Weldment, Meters.

Raussi and Tiainen^(12.2.14) investigated defect-size distributions in ferritic piping weldments. Specifically, they used RT on BWR. A-106 piping in the size 4.5 in. x 0.3 in. wall (114 mm dia x 8.6 mm t). The authors considered defect-size distributions at time of initial NDE, after rejection and after repair. Pores, slag inclusions, lack-of-fusion, lack-of-penetration, cracks, and tungsten inclusions were evaluated from the radiographs. While this study more correctly belongs in Chapter 13, it is included here because

FIGURE 12.2.6. Flaw-Size Distribution in Weldments

of the manner of examination and repair. The values may be biased to the low side in the event tight cracks existed. Figure 12.2.9 includes the initial defect-size distribution. Figure 12.2.10 combines the fitted curves covering defect-size distributions before rejection, after rejection and after repair.

Trumpfheller^(12.2.15) discussed extensive evaluations of weldments typical of reactor pressure vessels. One study covered approximately 2730 m (~9000 ft) of weld seam in two ranges of thickness. The first varied from 60 to 120 mm (2.3 to 4.7 in.) thick. Defect-size distributions are given in Figure 12.2.11 for 375 ultrasonically detected defects. The second varied from 120 to 250 mm (4.5 to 10 in.) thick and contained 240 defects. Figure 12.2.12 presents this defect-size distribution.

FIGURE 12.2.7. Number of Incomplete Penetrations as Function of Depth of Defect

FIGURE 12.2.8. Distribution of Defect Height and Type (~35 m of mild steel weldment, 10 to 26 mm thickness range)

FIGURE 12.2.9. Number of Imperfections in Weld Joints Detected With Radiography According to Their Size

FIGURE 12.2.10. Curves of Imperfection Size Distribution:
 (a) Before Inspection Rejection, $y(x)$;
 (b) After Inspection Rejection, $g(x)$;
 (c) After Repair of The Rejected Defects, $h(x)$

FIGURE 12.2.11. Distribution of Flaw Lengths for 375 Ultrasonic Flaw Indications, Wall Thickness: 60 to 120 mm

FIGURE 12.2.12. Distribution of Flaw Lengths for 240 Ultrasonic Flaw Indications, Wall Thickness: 120 to 250 mm

FIGURE 12.11. Distribution of flaw lengths for 375 ultrasonic flaw indications. Wall thickness: 80 to 120 mm.

FIGURE 12.12. Distribution of flaw lengths for 240 ultrasonic flaw indications. Wall thickness: 120 to 250 mm.

Hopefully, the data in Chapter 13 should yield a more definitive answer to the reliability of flaw detection by NDE with emphasis on UT. Until such data are available it will be necessary to utilize earlier studies. The two most meaningful are considered to be Marshall et al.^(12.2.3,12.2.4) and Becher and Hansen.^(12.1.3)

The Marshall report used a questionnaire sent to NDE experts to predict the probability of flaw detection as a function of flaw size. My subjective judgment based on PVRC experience is that such predictions tend to be overly optimistic both in predicting flaw size and in the reliability of detection of large flaws. Therefore, Figure 12.3.1 should be considered illustrative not definitive.

The study of Becher and Hansen^(12.1.3) is based on actual data derived from pressure vessels and evaluated with radiography. Again, the probability of detection of flaws in pressure vessels would be expected to be higher than in piping due to materials (often stainless steel in pipes versus ferritic steels), weldment geometry, and method of NDE (UT); for RT piping detection of flaw might be better.

Figure 12.3.2 presents the data from 347 radiographs. The significant factor is the shape of the curve. These data follow a log-normal distribution which isn't immediately obvious from either the cumulative distribution or the frequency (probability density) function.

Figure 12.3.3 is a plot of log flaw length versus cumulative percentage of flaws. In essence, this plot predicts that 97% of the flaws will be equal to or less than 100 mm long.

As cited previously, the trends observed should be considered as illustrative and not truly representative of the probability of detection in piping. Qualitatively, I would expect the fabrication flaw-size distribution in piping to be biased toward larger flaw-size ratios (a/t) compared to pressure vessels.

FIGURE 12.3.1. Diagram Showing a Plot of Probability of Not Detecting a Flaw ($\log_{10} B(x)$) Versus Crack Depth

The curves in Figures 12.3.1 and 12.3.2 can be combined to produce a modified flaw-size distribution representative of the distribution after repair of some of the detected flaws. The general shape of the curves is significant. Values of the equations have not been used, deliberately, to emphasize the lack of experimental bases for the curves given in Figure 12.3.4(a) and (b).

FIGURE 12.3.2. Distribution of Defect Size, Based on Examination of 347 Radiographs

The equations reported in Section 12.2 for initial flaw-size distribution can be modified to account for detection of flaws by NDE and for repair of those detected by NDE, when such repairs are deemed necessary. Lidiard and Williams^(12.2.3) developed a model based on the assumption that all detected flaws are repaired so that the population at startup is defined by the equation

$$N(X) = A(X)B(X) \quad (12.3.1(a))$$

FIGURE 12.3.3. Cumulative Distribution of Flaws Using IIW Degree for 347 Radiographs

where $N(X)$ represents flaw-size density distribution after repair, $A(X)$ the initial flaw population density function as noted in Section 12.2, and $B(X)$ the probability that defects of a given size will be missed:

$$A(X) = A \exp(-\lambda X) \quad (12.2.2(a))$$

$$B(X) = \epsilon + (1-\epsilon) \exp(-\mu X) \quad (12.3.2(a))$$

where $\epsilon = 0.005$ and $\mu = 2.88 \text{ in.}^{-1}$

$$A(X) = 14.8 \exp(-4.06X) \quad (12.2.2(b))$$

FIGURE 12.3.4. Comparison of Initial Fabrication-Induced Flaw Population and (A) Population After Repair, and (B) Flaws Detected by Nondestructive Examination

$$B(X) = 0.005 + (0.995) \exp(-2.8BX) \quad (12.3.2(b))$$

$$N(X) = A \exp(-\lambda X) [\epsilon + (1-\epsilon) \exp(-\mu X)]$$

$$N(X) = 0.0740 \exp(-4.06X) + 14.73 \exp(-6.94X) \quad (12.3.1(b))$$

Again, the validity of the various constants have not been established so the equations should be considered semiquantitative at best.

Harris^(12.2.5,12.2.6) uses a somewhat different approach in predicting nondetection of flaws. He uses error function notation to denote the probability as

$$P_{ND}(a) = 1/2 \operatorname{erfc}(\nu \ln a/a^*) \quad (12.3.3)$$

where a^* is the crack size that has a 50% chance of being detected, $\operatorname{erfc}(\lambda)$ is the complementary error function, and ν is an empirically derived parameter. The preceding equation is log-normal. Other possibilities include

$$\text{Weibull} \quad P_{ND}(a) = \exp(-a/\gamma)^\eta \quad (12.3.4(a))$$

$$a^* = \gamma(\ln 2)^{1/\eta} \quad (12.3.4(b))$$

$$\text{Gamma} \quad P_{ND}(a) = \exp(-\mu a) \sum_{n=0}^{k-1} \frac{(\mu a)^\eta}{n!} \quad (12.3.5)$$

where γ , η , μ , K , ν and a^* are defined parameters.

Some idea of the difference the three distributions yield can be seen in Figure 12.3.5. Harris used the work of Packman as evaluated by Tang on aluminum alloy tubing. While the results should not be extrapolated to ferrous tubing, the general trends are of interest.

FIGURE 12.3.5. Probability of Nondetection of a Crack as a Function of its Depth for an Ultrasonic Inspection. Example of spread using three distributions with same data.

The parameters γ , n , k , ν , μ and a^* may be used deterministically with single values or as continuous functions in which case medians and or means plus standard deviations are used. Table 12.3.1 summarizes distributions and values used by Harris.^(12.2.6) These values should be considered illustrative only because they depend on the model used plus the input assumptions. Some values apply to Sections 12.2 and to 12.4 through 12.8.

The implications of using the log-normal function for flaw detection and considering ν and a^* as continuous functions can be seen in Figure 12.3.6.

TABLE 12.3.1. Summary of Distributions of Input Parameters

Parameter	Type of Distribution	Deterministic Value	Mean (if given)	Median Value	Value of Standard Deviation (normal) "Second Parameter" (log-normal)
σ_j	normal	varies	0.8 x deterministic value	0.8 x deterministic value	0.155 x deterministic value
C	log-normal	2×10^{-10}	8.7×10^{-11}	4×10^{-11}	$\tilde{C} = 1.25$
a^*	log-normal	0.25	0.29	0.25	$\tilde{a}^* = 0.539$
v	normal	1.33	1.33	1.33	0.26
u	normal	$\ell_0 = 4:1.53$ 11:1.12 20:1.12	1.53 1.12 1.12	$\ell_0 = 4:1.5$ 11:1.12 20:1.12	$\ell_0 = 4:0.30$ 11:0.22 20:0.22
λ	log-normal	$\ell_0 = 4:0.00136$ 11:0.01 20:0.009		$\ell_0 = 4:0.00136$ 11:0.01 20:0.009	$\ell_0 = 4:\lambda=0.240$ 11:~0.76 20: 1.58
$P_V^*(\ell_0=4)$	log-normal	10^{-7}		10^{-9}	$\tilde{P}_V^*(\ell_0=4) = 4.95$

The following parameters are assumed to have fixed values: n_i (cycles of given transient), K_{IC} ; a_c ; $n(da/dN = C\Delta K^n)$.

In many instances of the parameters cited in the table, only $\hat{\sigma}_1$ and C are handled as distributed parameters. The others (λ , u , v , a^* , P_V^*) are considered constants.

FIGURE 12.3.6. Various Percentiles of Probability of Flaw Nondetection ($P_{ND}(a)$) as Calculated by Monte Carlo Using Distributed Values of v and a^*

FIGURE 15.3.3. Various Percentiles of Probability of Flaw Non-detection
 (P_{nd}) as Calculated by Monte Carlo using distributed
 Values of v and λ

The growth of new cracks by IGSCC, etc., will be discussed in Section 12.5. This section will concentrate on the fatigue growth of existing cracks due to the transients experienced by reactors. The approach presented here closely parallels that of Appendix A of ASME Section XI. The basic equation is

$$da/dN = C_0 \Delta K^\eta \quad (12.4.1(a))$$

where da/dN is the fatigue crack growth per cycle of loading; C_0 is a constant; and ΔK is the range of applied stress intensity factors, used in computing fatigue crack growth. The maximum range of K fluctuates for each transient, and η is the slope of the fatigue crack growth curve (log-log plot).

The above equation can be modified in terms of

$$\Delta K = (D_1 + D_2) (a)^{1/2} \quad (12.4.1(b))$$

where $D_1 = \Delta \sigma_m M_m (\pi/Q)^{1/2} \quad (12.4.1(c))$

$$D_2 = \Delta \sigma_b M_b (\pi/Q)^{1/2} \quad (12.4.1(d))$$

where $\Delta \sigma_m$, $\Delta \sigma_b$ are membrane and bending stresses; and M_m , M_b are membrane and bending correction factors, as determined from Figures A-3300-2 through A-3300-5 in Appendix A of ASME XI. Q is a flaw shape parameter, as determined from Figure A-3300-1 using the quantity $(\sigma_m + \sigma_b)$ and the flaw geometry.

Nilsson^(12.4.1) suggests a modification to the basic fatigue equation in line with probabilistic modeling. He suggests handling C_0 , which varies from environment to environment, as a probability density function. Although C_0 can be evaluated from experimental data, it often is necessary to make some

arbitrary assumptions concerning the behavior of C_0 in the absence of such data, so the net gain in considering C_0 a variable is marginal. Variations in assumed transients are much more significant.

Another approach is to consider ΔK^n as in a statistical distribution related to some Gaussian process: In the absence of relevant data the application of such an approach should be deferred.

Piping can generate a new population of flaws that may pass through the initiation period relatively rapidly, then grow to much larger sizes than is true of the flaw population during fabrication. This "operationally related" flaw population will be discussed in Section 12.5. Flaw growth will depend on

- material (austenitic, ferritic)
- environment (O_{H_2O} in BWR versus H_{H_2O} in PWR)
- residual stress
- sensitization (of austenitic)
- operational loads
- piping size
- piping systems.

The most common crack growth mechanism has been intergranular stress corrosion cracking (IGSCC), usually in smaller (4 to 12 in.) sizes of piping in BWRs.

The function describing crack growth in Section 12.5 may not be linear with time. IGSCC is dependent on the accessibility of the coolant to the root of the crack. It has been postulated, but not proven experimentally, that most IGSCC growth may occur during the reactor start-up or cool-down cycle. This means that 90% or more of the crack growth might occur in less than 1% of the operating cycle.

Design transients may or may not parallel the number and type of transients experienced by reactors. Neither the design nor the actual values are fixed. The design values have changed substantially over the past 5-10 years to parallel more closely operating experience while retaining a reasonable degree of conservatism. Table 12.4.1 contains a listing of transients used in relatively late studies. There is no assurance that the transients listed represent the current "official" list.

TABLE 12.4.1(a). Summary of Babcock & Wilcox Reactor Coolant System Transients (40-Year Design Life)

Transient	Description	Classification	Design Cycles
1	Heatup and cooldown 50°F/h heatup and cooldown	Normal	240
2	Power change 0 to 15% and 15 to 0%	Normal	730 230
3	Power loading, 8 to 50% power Power loading, 15 to 100% power	Normal Normal	3,000 15,000
4	Power unloading, 100 to 8% power Power unloading, 100 to 15% power	Normal Normal	3,000 15,000
5	10% Step-load increase	Normal	20,000
6	10% Step-load decrease	Normal	20,000
7	Step-load reduction from 100 to 8% power	Upset	
	Resulting from turbine trip		150
	Resulting from electrical load rejection		150
	TOTAL		300
8	Reactor trip	Upset	
	Type A - (Loss of reactor coolant flow)		120
	Type B - (Turbine trip without control action)		140
	Type C - (Loss of main feedwater flow)		120
	Trips included in transient numbers 11, 15, 16, 17, and 21		122
	TOTAL		502
9	Rapid depressurization	Upset	40
10	Change of flow	Upset	30
11	Rod withdrawal accident	Upset	40
12	Hydrotests	Test	20
13	Steady-state power variations	Normal	Infinite

TABLE 12.4.1(a). (contd)

Transient	Description	Classification	Design Cycles
14	Control-rod drop	Upset	40
15	Loss of station power	Upset	40
16	Steam-line failure	Faulted	1
17A	Loss of feedwater to one steam generator	Upset	20
17B	Stuck-open auxiliary vent valve	Upset	20
18	Loss of feedwater heater	Upset	40
19	Makeup flow transients	Normal	
	Type A - feed-and-bleed operations		18,000
	Type B - miscellaneous		30,000
	Type C - steady-state variations		4×10^6
20	Spray transient	Normal	20,000
21	Loss of coolant	Faulted	1
22	Operational test transients		
	HP injection system	Normal	40
	Core flooding system	Normal	240
23	Steam generator filling, draining, flushing and cleaning	Normal	
	Secondary side filling		240
	Primary side filling		240
	Flushing		40
	Chemical cleaning		<u>20</u>
	TOTAL		540
24	Hot functional testing	Normal	5
25	Leak testing	Test	100
26	Safe shutdown earthquake	Faulted	1
27	One-half safe shutdown earthquake	Upset	650

TABLE 12.4.1(b). Summary of Combustion Engineering Reactor Coolant System Transients (40-Year Design Life)

<u>Normal Conditions</u>		<u>Number</u>
a. Plant heatup, 100°F/hr (56°C)		500
b. Plant cooldown, 100°F/hr (56°C)		500
c. Plant loading, 5%/min		15,000
d. Plant unloading, 5%/min		15,000
e. 10% Step-load increase)		
f. 10% Step-load decrease)		(10) ⁶
g. Normal plant variations)		
<u>Upset Conditions</u>		
h. Reactor trip		400
i. Loss-of-reactor coolant flow		40
j. Loss of load		40
k. One-half safe shutdown earthquake		200
<u>Emergency Conditions</u>		
l. Loss of secondary pressure		5
<u>Faulted Conditions</u>		
m. Normal operation plus SSE		
n. Normal operation plus SSE plus pipe rupture		
<u>Test Conditions</u>		
o. Component hydrostatic test		10
p. System leak test		200

TABLE 12.4.1(c). Summary of General Electric Reactor Coolant System Transients (40-Year Design Life)

<u>Normal, Upset, and Testing Conditions</u>	<u>Number of Events</u>
Bolt Up(a)	123
Design Hydrostatic Test	40
Startup (100°F/hr Heatup Rate)(b)	120
Daily Reduction to 75% Power(a)	10,000
Weekly Reduction to 50% Power(a)	2,000
Control-Rod Pattern Change(a)	400
Loss of Feedwater Heaters (80 Cycles Total):	80
50% Safe Shutdown Earthquake Event	1(d)
Scram:	
Turbine Generator Trip, Feedwater on, Isolation Valves Stay Open	40
Other Scrams	140
Bypass Loss of Feedwater Pumps, Isolation Valves Closed	10
Turbine Single Safety or Relief Valve Blowdown	8
Reduction to 0% Power, Hot Standby, Shutdown (100°F/hr Cooldown Rate)(b)	111
Unbolt	123
<u>Emergency Conditions</u>	
Scram:	
Reactor Overpressure with Delay Scram, Feedwater Stays on, Isolation Valves Stay Open	1(d)
Automatic Blowdown	1(d)
Improper Start of Cold Recirculation Loop	1(d)
Sudden Start of Pump in Cold Recirculation Loop	1(d)
Improper Startup with Reactor Drain Shut Off Followed by Turbine Roll and Increase to Rated Power	1(d)
<u>Faulted Condition</u>	
Pipe Rupture and Blowdown	1(d)
<u>Safe Shutdown Earthquake</u>	1(d)

(a) Applies to reactor pressure vessel only

(b) Bulk average vessel coolant temperature change in any 1-hour period

(d) The 40-year probability of the one cycle events is $<10^{-1}$

(c) Includes 10 maximum load cycles per event

TABLE 12.4.1(d). Summary of Westinghouse Reactor Coolant System Transients (40-Year Design Life)

Normal Conditions	Occurrences
1. Heatup and cooldown at 100°F/hr pressurizer cooldown 200°F/hr)	200 (each)
2. Unit loading and unloading at 5% of full power/min.	13,200 (each)
3. Step-load increase and decrease of 10% of full power	2,000 (each)
4. Large step-load decrease with steam dump	200
5. Steady-state fluctuations	
a. Initial fluctuations	1.5×10^5
b. Random fluctuations	3.0×10^6
6. Feedwater Cycling at Hot Shutdown	2,000
7. Loop Out of Service	
a. Normal Loop Shutdown	80
b. Normal Loop Startup	70
8. Unit Loading and Unloading Between 0 and 15% of Full Power	500 (each)
9. Boron Concentration Equalization	26,400
10. Refueling	80
11. Turbine Roll Test	20
12. Primary Side Leak Test	200
13. Secondary Side Leak Test	80
14. Tube Leakage Test	800
<u>Upset Conditions</u>	
1. Loss of Load, Without Immediate Reactor Trip	80
2. Loss of Power (Blackout With Natural Circulation in the Reactor Coolant System)	40
3. Partial Loss of Flow (Loss of One Pump)	80
4. Reactor Trip from Full Power	
a. Without cooldown	230
b. With cooldown, without safety injection	160
c. With cooldown and safety injection	10
5. Inadvertent Reactor Coolant Depressurization	20

TABLE 12.4.1(d). (contd)

Normal Conditions	Occurrences
<u>Upset Conditions (contd)</u>	
6. Inadvertent Startup of an Inactive Loop	10
7. Control-Rod Drop	80
8. Inadvertent Emergency Core Cooling System Actuation	60
9. Operating Basis Earthquake (20 Earthquakes of 10 Cycles Each)	200
<u>Emergency Conditions^(a)</u>	
1. Small Loss-of-Coolant Accident	5
2. Small Steam-Pipe Break	5
3. Complete Loss of Flow	5
<u>Faulted Conditions^(a)</u>	
1. Main Reactor Coolant Pipe Break (Large LOCA)	1
2. Large Steam-Pipe Break	1
3. Feedwater Line Break	1
4. Reactor Coolant Pump Locked Rotor	1
5. Control-Rod Ejection	1
6. Steam Generator Tube Rupture	Included Under Upset Conditions, Reactor Trip from Full Power With Safety Injection)
7. Safe Shutdown Earthquake	1
<u>Test Conditions</u>	
1. Primary Side Hydrostatic Test	10
2. Secondary Side Hydrostatic Test	10

(a) In accordance with the ASME Nuclear Power Plant Components Code, Emergency and Faulted Conditions are not included in Fatigue Evaluation.

The development of the equations for both crack initiation and for crack growth is covered in Section 12.5.

These transients can be decoupled so that the order of occurrence does not influence crack growth. Inherently, the assumption is that the preceding cycle does not affect the crack growth from the following cycle and, therefore, that cycle ordering can be neglected. This may be affected by the volume of the plastic zone at the crack tip and by the access of the coolant to the crack tip, assuming the crack reaches the inner surface.

The rate of crack growth is influenced by location (surface versus embedded), stress cycles, access to coolant, size and orientation. However, crack growth rates are relatively low except under the most severe conditions. Table 12.4.2 illustrates the slow rates of crack growth. In this table, and in the following tables, emphasis is given to hot-leg and cold-leg piping in PWRs. The shorter time for penetration from the inner surface is due to the larger (factor of 14.7) value of C_0 in the "wet" condition.

A Combustion Engineering study^(12.4.2) examined crack growth in the hot-leg and cold-leg piping using the C.E. design transients. Data are given in Table 12.4.3. The values found in Table 12.4.3, not too surprisingly, tend to parallel those in Table 12.4.2.

The same study^(12.4.2) examined the number of cycles to cause leakage and then to cause failure after leakage, Table 12.4.4. Heatup and cooldown cycles, plus relatively severe reactor trip and loss-of-flow cycles were used to propagate the cracks. Typically, there are about 500 heatup and cooldown cycles in 40 years, the assumed reactor life. An initial crack depth of 1.0 in. (presumably on the inner surface) was assumed with three crack lengths--8, 16, 24 in.

The same trends observed in B&W and CE piping occur in W Plants, Table 12.4.5.^(12.4.3) This study, and those in references 12.2.5, 12.2.6, 12.4.2, were prepared in response to the asymmetric load problem where a guillotine break assumed at the nozzle safe-end region of PWR hot legs or cold legs can pressurize the pressure vessel cavity, resulting in asymmetric loading during blowdown. The W study was on 30-in. I.D., 2.5 in. thick wall, 316 stainless steel pipe. Three flaws were assumed (1) O.D. circumferential;

TABLE 12.4.2. Time for Crack to Grow Through the Wall of Large Diameter Piping (12.2.5, 12.2.6)

Initial Conditions

Initial Flaw Depth $a_0 = 0.25$ in. unless noted $a/l < 0.1$

Wall thickness (Ferritic Piping): B&W 3.3125 in.
CE 3.75 in.

Maximum Stress Fatigue Growth and Cycles:

B&W 13.0 KSI - ~400 cycles/yr

CE 45.5 KSI - 17 cycles/yr

Flaw Location	Stress Field	Time to Failure ^(a) Years	
		CE	B&W
Inner Surface	Uniform	8.9	20.8
		22.1	53.5 $a/l = 0.2$
		>56.2	>140 $a/l > 0.35$
Inner Surface	Gradient (Tensile on ID)	12.6	47.4
Outer Surface	Uniform	126	295
Mid Plane	Uniform	645	1560
1/4 thickness		618	1420

(a) Failure defined as time to penetrate the wall (t_f)

TABLE 12.4.3. Crack Growth in Piping in 100 Years Assuming Design Transients (12.4.2) Initial Crack Depth $a_0 = 1.0$ in. Lengths as Noted (Units are inches)

System	Orientation	$l_0 = 8$ in.	$l_0 = 18$ in.
Hot Leg	Circumferential	~1.1	~1.12
	Axial	~1.05	~1.1
Cold Leg	Circumferential	~1.01	~1.03
	Axial	~1.03	~1.1

Note: Values represent final crack depth.

TABLE 12.4.4. Cycles to Leak and to Cause Failure in Hot-Leg and Cold-Leg Piping (12.4.2)

Flaw Length, in.	Cold Leg	Hot Leg	
24	~2,500	~7,000	To Leak
16	~3,000	~9,000	
8	~8,000	~16,000	
Axial Crack			
18	5-10 (~22 in.)	5-10 (~22 in.)	To Break After Leaking Cycles; (length at Break)
12	30-40 (~18 in.)	~75 (~22 in.)	
8	160 (~18 in.)	~280 (~22 in.)	
Circumferential Crack			
18	~150 (~32 in.)	~200 (~32 in.)	
12	~400 (~32 in.)	~500 (~32 in.)	
8	~1200 (~32 in.)	~1000 (~32 in.)	

(2) I.D. circumferential; (3) O.D. axial. The same flaw size and geometry was assumed; namely, $a_0 = 0.5$ in., c/a ratio of 6. In passing, the critical flaw for failure was 30-in. long for the axial case and >30-in. long for circumferential, both through-wall.

The values in Table 12.4.5 are in agreement with the CE values in Table 12.4.4.

A question of interest is, "How large can an initial flaw size be to become critical when exposed to operational transients for a given time?" Table 12.4.6 presents data from Harris^(12.2.5, 12.2.6) for B&W and CE plants. In the case of B&W, the critical transient is 3-4a power change up or down from 8% to 100%. 15,000 were used for the 40-year period together with the other cycles. Typical CE cycles were used.

TABLE 12.4.5. Growth of Flaws as Function of Size and Location in Outlet Piping ~29-in. I.D. x 2.5-in. Wall 316 Stainless Steel c/a Ratio 6

Initial Postulated Flaw Depth (in.)	Length	Total Flaw Depth (in.) for Times												
		Time-Yr.	3			6			10			40		
			(1)	(2)	(3)	(1)	(2)	(3)	(1)	(2)	(3)	(1)	(2)	(3)
0.100	0.600	0.100	0.100	0.100	0.100	0.100	0.101	0.100	0.101	0.102	0.102	0.103	0.107	
0.200	1.200	0.201	0.201	0.202	0.201	0.202	0.204	0.202	0.203	0.206	0.208	0.212	0.228	
0.300	1.800	0.301	0.302	0.304	0.303	0.304	0.309	0.305	0.307	0.315	0.320	0.328	0.368	
0.400	2.40	0.403	0.404	0.408	0.405	0.407	0.416	0.409	0.412	0.427	0.440	0.454	0.535	
0.500	3.00	0.505	0.506	0.512	0.509	0.512	0.525	0.515	0.520	0.544	0.569	0.594	0.749	
0.700	4.20	0.710	--	--	0.722	--	--	0.737	--	--	0.898	--	--	

- (1) Circumferential Crack on O.D. Surface.
 (2) Circumferential Crack on I.D. Surface.
 (3) Axial Crack on O.D. Surface.

TABLE 12.4.6. Acceptable Initial Flaw Sizes (a_0) inches for Hot Leg, Cold Leg and Surge Lines in B&W and CE Plants, Using Design Transients

Operating Years	Hot Leg		Cold Leg		Surge Lines	
	B&W	CE	B&W	CE	B&W	CE
10	0.40	0.22	0.60	0.65	0.10	0.03
20	0.25	0.14	0.40	0.52	0.07	0.02
30	0.20	0.10	0.35	0.42	0.05	0.01
40	0.10	0.09	0.30	0.39	0.05	0.01

The surge lines usually are 9 - 11 in. I.D. with wall thickness of ~1.0 in.

One further item of interest is the length of a through-wall crack necessary to cause a guillotine break or major axial split under substantial internal pressure loads.

Figure 12.4.1 from reference 12.4.3 covers the case of failure pressures for circumferential through-wall flaws. Table 12.4.7 contains crack lengths for hot leg, cold leg, and surge line in B&W and CE plants.

Figure 12.4.2 cited by Wilson^(12.2.2) presents data similar to that of Figure 12.4.1 for axial rather than circumferential flaws. The trends are similar to those noted in Figure 12.4.1.

FIGURE 12.4.1. Failure Pressure for Circumferential Through-Wall Flaws

TABLE 12.4.7. Critical Crack Length for Guillotine Failure for a Leaking Crack

Joint	$\sigma_{\max}/\bar{\sigma}$	$\lambda = Q/\sqrt{RT}$	Length	
			B&W	CE
Hot Leg	0.72	2.0	16.8	19.4
Cold Leg	0.58	3.6	24.8	26.6
Surge Line	1	--	~4	~4

Note: These are high-stress levels analogous to faulted conditions.

FIGURE 12.4.2. Distribution of Data About Critical Stress Prediction Plane for Axial Failure in Piping

FIGURE 12.4.9. Distribution of Data About Critical Stress Prediction
Plane for Axial Failure in Piping

The significance of initial flaw-size distribution in piping welds probably has been exaggerated, particularly when the flaws usually occur between $1/4t$ and $3/4t$ (the central half of section thickness). The driving forces for crack propagation are relatively low for small deeply embedded flaws, not exposed to the coolant. The situation is different for surface or near-surface flaws, particularly those distributed around the inner surface. In such flaws it may not be necessary to assume a prior flaw-size distribution. Under certain circumstances, the driving forces are sufficiently high to result in both crack initiation and crack growth with the cumulative time for both stages less than predicted for the growth of embedded flaws when the two processes are compared against the same final flaw size.

Three mechanisms leading to crack initiation and growth will be considered. Due to the absence of relevant data the mechanisms will be modeled, but no effort will be made to determine numerical values. The following are three mechanisms, all of which have resulted in piping failures in operating BWRs and/or PWRs:

1. stress corrosion cracking, primarily intergranular stress corrosion cracking (IGSCC). (This is predominantly but not exclusively a BWR problem occurring in austenitic weldments.)
2. fatigue cracking, initiated by thermal-transients and continued by combined thermal and pressure loads.
3. corrosion-fatigue, where cracks initiate and grow from the inner surface due to a combination of the environment and the cyclic loads.

12.5.1 Intergranular Stress Corrosion Cracking (IGSCC)

Relatively little has been reported relevant to incorporating IGSCC into a probability failure model. A recent study by Harris^(12.5.1) attempts to attack the problem; however, that study begins with the a priori assumption of a flaw population so that the system is propagation controlled. In my opinion this introduces undue restrictions into the model.

The crack growth by IGSCC can be divided into two stages--the initiation or nucleation period, and the crack propagation period. While a microscopic model probably is better for describing crack nucleation, a deliberate decision was made to use a macroscopic model to be consistent with the crack-growth models used in most probability studies.

Extensive studies in BWRs and simulated BWR environments indicate that the following parameters are controlling for austenitic weldments:

1. the environment (E) with the level of oxygen in the coolant (at temperature) usually controlling
2. the degree of sensitization (S) in the wrought structure (heat affected zone)
3. the level of stress (σ) at the inner surface (σ_{res} , $\sigma_{D.W.}$, σ_p , etc.)
4. the condition of the internal surface (γ) in the region of stress and sensitization

The time-to-crack nucleation or initiation will be a complex function of these and possibly other parameters. For example, the process has been arbitrarily limited to austenitic alloys even though it is known that certain austenitic alloys are relatively insensitive to IGSCC while some of the high nickel alloys are relatively susceptible. Times of initiation may range from days to months, or even years, based both on experimental evidence and observations with operating reactors. We can define this initiation as

$$T_{init}(\text{hrs}) = f(E, S, \sigma, \gamma)$$

where stress may be steady state plus transient, and may consist of residual, dead weight, pressure, bending, etc. With regard to surface condition γ , one could argue that a rough surface corresponds to a population of small cracks, thus shortcutting the initiation stage.

The preceding relationship could be defined in terms of a probability relationship; however, no analytic procedure is known for quantifying the relationship, primarily because of the very limited data on initiation of IGSCC and

equally limited information concerning the interaction of E , S , σ , γ . One can infer that the presence of some level of oxygen in the water at 250 to 300°C and a nominal level of sensitization both represent plateaus where further increases of E or S are relatively unimportant. With this assumption, σ and γ effectively control T_{INIT} . The relative contributions of σ and γ are difficult to assess.

The post-initiation step is followed by crack growth with the same parameters controlling, or

$$\text{Crack growth rate} = a = f'(E, S, \sigma, \gamma),$$

where a is a rate function which probably is not uniform and may be discontinuous.

For example, the effective E will be that at the crack tip; however, that E will be influenced by the macroscopic E , the level of oxygen in the coolant. Typically BWR's start with fairly high levels of oxygen at startup with these levels reduced markedly before reaching operating (P , T); however, some BWR's have reduced the oxygen levels prior to ascent to operating conditions, and these plants have been essentially free of IGSCC, at least to date.

It has been postulated that the crack growth occurs primarily during startup and shutdown. The suggested mechanism particularly relevant to startup is that oxygen is present, and loads during heatup may be substantial, particularly bending loads that could open the cracks permitting access of coolant. If such is true, one might have 90 to 95% of the crack growth in <1% of the time during a 300-day operating cycle.

The preceding is conjecture; it would be beneficial to have definitive data confirming or denying the postulate.

The report by Harris^(12.5.1) cited previously contains an extensive model of IGSCC. However, some of the basic assumptions introduced initially (for example, the assumption of an a priori flaw population) bias the model severely, at least in my opinion. Even so some of the conclusions are considered relevant. This study considered two sizes of pipe, a 60-cm (24-in.) recirculation line and a 10-cm (4-in.) surge line:

- The residual stresses distribution tends to decrease the probability of failure in the large line while increasing it in the small line. This is due to the distribution circumferentially and radially of the residual stresses.
- Other parameters, such as maximum crack growth rate, maximum anticipated stress intensity factor, K_{ISCC} intensity factor, and slope (m) of a-K relation, have secondary effects with regard to failure probability.
- Stresses typical of emergency or faulted conditions may increase probability of failure and reduce time to failure; however, the changes are expected to be nominal. This is generally true for transient loads.
- Preservice and inservice NDE have limited effects on failure probability due to IGSCC.
- Calculated failure rates (which assume no initiation stage) predict high rates in short times. This does not agree with field observations, suggesting that nucleation may be important.

Egan and Cipolla^(12.5.2) developed a crack growth model for IGSCC. Their model, like Harris', assumed prior existence of a crack so only the propagation stage was considered. The following represent their assumptions:

- Plant and hence stress cycling has no other effect on growth rate provided the crack-growth data are determined from tests which include representative loading (i.e., cycling comparable to service loading).
- $1/a_f$ is much smaller than $1/a_i$ and may be neglected.
- $da/dt = CK_{max}^n$ is a reasonable law to describe stress corrosion crack growth (i.e., stress corrosion crack propagation is a stress intensity dominated phenomenon for the time of interest).
- Post-weld grinding affects only the initiation phase of stress corrosion cracking and has no influence on crack propagation.

- Initiation or incubation of stress corrosion cracks is an independent phenomenon and is not considered in the propagation analysis.
- The material is homogeneous and can be treated as a continuum.

The authors conclude that 1) propagation times are much longer in large pipes; 2) the more highly stressed laboratory pipes will fail much more rapidly than similar types of pipes in the field; and 3) the substitution of ferritic piping would require toughness values at least $42 \text{ ksi(in.)}^{1/2}$ for leak before break to be guaranteed.

12.5.2 Thermal Fatigue

A thermal fatigue model assumes a large number of stress cycles of sufficient severity to initiate and then propagate cracks. Such fatigue is attributed to inadequate mixing in the coolant so that a cooler stream periodically impinges on the inner surface. Such fatigue has been experienced in nozzles, elbows, etc. Generally, the thermal fatigue results in relatively rapid initiation of cracks and their propagation to nominal depths. The thermal stress driving force for continued propagation drops off with increased depth. Other mechanisms, such as transient loads or periodic variations in operating stresses, are postulated to cause continuous crack growth. The significant factors here are σ and possibly γ . Both ferritic and austenitic materials can crack; however, the higher coefficient of expansion of austenitics tends to increase the probability of cracking in these materials under conditions marginal to crack initiation.

12.5.3 Corrosion-Fatigue

Another mechanism for initiation and propagation of cracks is corrosion-fatigue applicable to both ferritic and austenitic alloys. Here cyclic stresses, their amplitudes and number of cycles tend to control. Exposure to the environment tends to increase crack growth rates and presumably, reduce crack initiation times by factors of 10 - 100, depending on material, condition of environment, ratio of stress amplitudes, etc. Here the driving force continues since the cycles usually are stress controlled across the cross section rather than being a decaying gradient.

Cramond^(12.1.7) discusses fatigue crack initiation with emphasis on pressure vessels. His models consider such initiation generically rather than assessing environmental effects. Basically, they are modifications of existing models covering the propagation of cracks under cyclic loads.

For example, Miner's rule has been modified analytically to cover crack initiation. Here D_0 defines crack initiation of engineering significance:

$$D_0 = \sum_{\ell=1}^{\ell} \frac{N_{\ell}}{N_0^{\ell}} ; D_0 > 1.0 \quad (12.5.1(a))$$

where N_{ℓ} = no. of load cycles of stress level

N_0 = crack initiation life

W = total no. of load cycles until crack initiation

$$\text{or } D_0 = \sum_{j=1}^W \frac{1}{N_{0j}} \quad (12.5.1(b))$$

$$\text{or } D_0 = \sum_{j=1}^W D_{0j} \quad (12.5.1(c))$$

where D_{0j} = initiation damage incurred during load cycle, j .

In terms of fatigue cycles,

$$N_0 = N_f - C_1 N_f D_0 \quad (12.5.2)$$

where C_1 , D_0 are material constants and the second term is propagation life:

N_0 - crack initiation life in terms of cycles

N_f - cyclic life

N_0 is obtained either analytically or experimentally on the basis of the number of cycles necessary for a crack to propagate from the initial "engineering crack size" to a final crack causing failure.

Another approach to predicting initiation life is through use of the law of universal slopes. Coefficients and exponents are determined experimentally from fatigue tests.

D_0 , again, is used to denote initiation of a crack of engineering size. There still is the problem of exact solution to determine means (W) and coefficients of variation Ω_W denoting the number of cycles for crack initiation. $\bar{W}$ is defined as the average crack initiation life including effects of cumulative damage.

N_0 is the cyclic initiation life under constant amplitude alternating stresses ($R = -1$), generally not applicable in real systems. W is defined as the cyclic initiation life under random alternating stresses.

12.5.4 Combined Crack Initiation and Growth

Besuner and Tetelman^(12.1.1) developed a probabilistic model for crack growth that should be applicable to piping on the basis that the critical flaw size is quite large and the initial flaw is presumed to quite small. This model is capable of handling the case of flaws initiating during operation by IGSCC, etc.:

$$da/dN_{pr} = C \Delta K^n \quad (12.5.3(a))$$

where N_{pr} represents a summation of all transient cycles.

The simplest flaw was considered; namely, a buried penny-shaped crack remote from the surface in a uniform tensile stress field,

$$\Delta K = 2 \Delta \sigma (a/\pi)^{1/2} \quad (12.5.3(b))$$

where $\Delta \sigma$ represents the stress range.

Substitution of (12.5.3(b)) into (12.5.3(a)) and integration yields

$$N_{pr} = \frac{a_i^{(1-n/2)} - a_f^{(1-n/2)}}{C \Delta \sigma^n (4/\pi)^{n/2} (n/2 - 1)} \quad (12.5.4(a))$$

where $n \neq 2$, a_i is initial, and a_f is final half-crack size, and $a_f \gg a_i$.

If $a_f \gg a_i$ (12.5.4(a)) reduces to

$$N_{pr} = \frac{(A_i/\pi)^{(1/2 - n/4)}}{(\Delta \sigma)^n (4/\pi)^{n/2} (n/2 - 1)} \quad (12.5.4(b))$$

where $A_i = \pi a_i^2$ represents the flaw area.

The error in assuming a circular flaw versus an elliptical flaw is nominal; e.g., if the ratio of major to minor axes is <40 the difference is not significant.

Equation (12.5.4(b)) can be converted to more compact notation and logarithms taken:

$$\log N = \log B + p \log C + q \log \Delta \sigma + r \log A_i \quad (12.5.5)$$

where $B = \left[2^{-n} \pi^{(-1/2 + 3n/4)} (n/2 - 1) \right]^{-1}$

$$p = -1$$

$$q = -n$$

$$r = 1/2 - n/4$$

and B is a constant not subject to random variation.

Returning to

$$da/dn = C \Delta K^n \quad (12.4.1)$$

we can substitute functions

$$\log C = \bar{N}(\mu_{C_1}, S_{C_1})$$

$$\log \Delta\sigma = \bar{N}(\mu_{\sigma}, S_{\sigma})$$

$$\log A_i = \bar{N}(\mu_{A_i}, S_{A_i})$$

where μ represents the median or mean, and S is the standard deviation. Values for μ and S can be developed.

If the design life is defined as

$$Y = N_{pr} = N_{pr}(C, \Delta\sigma, A_i) \quad (12.5.5)$$

and the probability of failure is

$$P_f = P(Y < \text{design cycles}) \quad (12.5.6)$$

to solve the theorem

$$X_1 = \bar{N}(\mu_1, S_1), X_2 = \bar{N}(\mu_2, S_2)$$

etc., is used, and

$$Y = P_1 X_1 + P_2 X_2 \dots + P_3 \quad (12.5.7)$$

where P_1, P_2, P_3 are constants:

$$Y = \bar{N} (\mu_y, S_y) \quad (12.5.8)$$

$$\mu_y = P_1 \mu_1 + P_2 \mu_2 + P_3 \mu_3 \quad (12.5.9)$$

$$S_y = (P_1^2 S_1^2 + P_2^2 S_2^2)^{1/2} \quad (12.5.10)$$

$$\log N_{pr} = \bar{N} (\mu_N, S_N)$$

$$\mu_N = \log B\mu + P_{\mu_C} + q\mu_{\sigma} + r\mu_A$$

$$S_N = (P_{\mu_C}^2 S_C^2 + q^2 S_{\sigma}^2 + r^2 S^2)^{1/2}$$

For nonzero initiation life,

$$N = N_i (\Delta\sigma, A_i, D) + N_{pr} (\Delta\sigma, A_i, C) \quad (12.5.11)$$

$$\log D = \bar{N} (\mu_p, S_p)$$

and

$$N = M \frac{D}{A_i^k \Delta\sigma^{\ell}}$$

Recalling that crack growth is denoted by an equation of the form

$$\frac{da}{dN} = C (\Delta K)^{\eta}$$

$$\Delta K = \Delta \sigma h_j(a)$$

$$\int_{a_0}^{a(t)} \frac{da}{h_j(a)^\eta} = C \sum_{K=1}^{N(t)} \Delta \sigma_K^\eta$$

If N represents the number of fatigue cycles of significance, this number probably won't be sufficiently large enough to meet the criteria of a central limit theorem.

The probability density function for the size distribution of flaws in the original flaw population that have grown after some time is

$$f_a(v) = \frac{1}{[H_j(v)]^\eta} f_c I_N [S(v, a_0)]$$

where v represents the flaw depths at time (t) ; then

$$f_c I_N \text{ will be } \sum_{u=0}^{\infty} f_c(u) f_{I_N} \left(\frac{z}{u} \right) \frac{du}{u}$$

New flaws originating due to the material, the environment, the stress levels and the stress cycles will be of the form

$$\frac{da}{dt} = f(\text{Mat, Env, } \sigma, \Delta \sigma_K^\eta)$$

This equation should represent growth both due to constant stresses (σ , σ_{res} , etc.), IGSCC and to cyclic stresses.

etc.), LSCC and to cyclic stresses.

This equation should represent growth both due to constant stresses (σ , σ_{res}

$$\frac{da}{dt} = f(\text{Mat., Env., } \sigma, \Delta\sigma, n)$$

and the stress cycles will be of the form

New flaws originating due to the material, the environment, the stress levels

$$C_N \text{ will be } \sum_{i=0}^{\infty} C_N(u) f_N\left(\frac{z}{u}\right) \frac{du}{u}$$

where v represents the flaw begins at time t , then

$$f_N(v) = \frac{1}{N} \frac{1}{C_N} \left[2(v, \sigma_0) \right]$$

the original flaw population that have grown after some time is

The probability density function for the size distribution of flaws in

theorem.

only won't be sufficiently large enough to meet the criteria of a central limit

It N represents the number of fatigue cycles of significance, this number prop-

$$\int_0^{\infty} f(a) \frac{da}{H(a)^n} = C \sum_{k=1}^N \frac{H(a_k)}{a_k^n}$$

$$\Delta k = \Delta \ln(a)$$

12.6.1 Load Effects

The effect of loads is twofold. They, in combination with other factors such as the environment, are responsible for the propagation of cracks to a greater or lesser degree. Ultimately, a load is necessary to result in failure either due to leakage, or catastrophically.

In the crack initiation and propagation stage, the probability of crack growth is sensitive not only to $f(E, S, \sigma, \gamma)$ for IGSCC, but also to piping diameter and to piping subsystems. It is necessary to examine a spectrum of piping size to meaningfully predict crack growth as noted by Harris^(12.5.1) for 10-cm and 60-cm pipe. In this respect, the work of Wilson^(12.2.2) on factors influencing conditional probability of failure is decidedly relevant. Figure 12.6.1 illustrates the contribution of primary and secondary stresses to relative probability of failure, and Figure 12.6.2 examines geometric and size factors influencing probability of failure. Both support the significance of pipe size and of subsystem to failure.

Another method of presenting the data in Figure 12.6.2 is given in Figure 12.6.3 where a probability surface is drawn (qualitatively) to examine the role of pipe size and relevant piping system to failure probability.

Another factor influencing probability of failure is the rate of loading as well as the maximum load. For example, faulted loads predicted by one mechanism may be quasi-static while faulted loads predicted by another may be dynamic. A specific example might be certain seismic loads where the ramp approximates quasi-static conditions and a region of high-stress intensity such as a discontinuity might yield but not fail. A dynamic faulted load might be the opening of safety relief valves or water slugging.

Table 12.6.1^(12.2.1) illustrates a few cases of pipe damage ranging in severity from complete failures to limited local damage. The probability function in Figure 12.6.4 attempts to relate load to piping size for static and dynamic loads. Figure 12.6.4 is a two-dimensional section of Figure 12.6.3 with the effects of dynamic loading added.

FIGURE 12.6.1. Relative Probability of Severance by General Fault Cause (Stress Category)

FIGURE 12.6.2. Relative Probability of Severance by Component-With-Joint Pipe Size and Crack Orientation

Basically, an increase in strain rate will have the following effect:

$$> e \sigma_y < K_{IC} < K_m$$

where > denotes an increase in value and < denotes a decrease in value.

FIGURE 12.6.3. Probability a Given Load will be Reached or Exceeded for a Spectrum of Emergency and Faulted Incidents

12.6.2 Probabilistic Modeling of Loads

There has been substantial work in probabilistic modeling of structures; however, the information available on the modeling of loads in piping systems is quite limited. Since this report concentrates on piping systems, the discussion of probabilistic modeling of loads will be limited to work in its early stages, in 1979. (12.6.1)

The authors' attempt to develop a model within accepted practice. To accomplish this, they established three requirements:

1. The designer or reviewer should only have to combine peak responses from individual loads.
2. The response combinations in requirements in 1 should be specified in simple deterministic terms.

TABLE 12.6.1. Incidents Considered of Substantial Significance Based on Size and Location of Piping (1970-1975)

<u>Reactor</u>	<u>Year</u>	<u>Incident</u>
H. B. Robinson	1970	During hot functional testing, a 6-in. (152-mm) pipe nozzle between the pressurizer main steam line and the safety valve failed completely due to crack initiated at excessively machined taper.
Dresden 2	1970	Water hammer in high-pressure coolant-injection (HPCI) line due to water behind valve; dented 10 ft (3 m) of pipe and broke or bent several restraints and snubbers.
Indian Point 1	1972	Thermal sleeve in 4-in. (102-mm) line failed by thermal fatigue; in turn the mixed flow cracked the 24-in. (609-mm) main coolant line downstream owing to thermal fatigue.
Turkey Point 3	1972	During functional testing, three of four safety valves on the main steam-line header blew off and split the header; caused by system not being designed for reaction forces under dynamic loads.
Indian Point 2	1973	Water hammer due to uncovering of steam generator feed-water ring cracked feedwater line 180° circumferentially and bulged pipe.
Browns Ferry 1	1974	Water hammer due to operator error moved HPCI pipe, broke 3 pipe restraints, bent and damaged others; fractured journal-bearing pedestal.
Fitzpatrick	1974	Water hammer due to operator error moved parts of residual-heat-removal pipe up to 10 in. (254 mm) and damaged seismic supports.
Cooper	1975	Coupling on control-rod-drive pump impacted and gouged 10-in. (254-mm) core spray line; caused by bearing failure and pump shaft breaking.

3. For mechanical components the ASME code stress limit and service level (A, B, C, D) philosophy should be used.

Particular attention was given to the specification of loads, load combinations and load factors. The following questions were asked:

FIGURE 12.6.4. Section of Figure 12.6.3 Representing One Piping Complex Illustrating Differences of Effective and Static Loads

- Loads

- What are the sources of and uncertainties in the loads?
- What are the probabilities of occurrence of different load intensities?
- What are the durations of the loads?
- Is the loading function or displacement controlled?
- What are the dynamic characteristics of the loads?

- Load Combinations

- What is the probability of two or more concurrent loads?

- What consequences can be allowed due to concurrent loads? (This determines the equipment service level or structural load acceptance criteria.)
- What is the probability that the specified consequences (deformation, cracking, etc.) would be exceeded with the specified load combination?
- What is the effect on safety for the specified load combinations (sensitivity of overall risk)?
- What response combination criteria is to be associated with the selected load combination criteria?
- Load Factors
 - What is the nature and mode of the load; i.e., is it static, vibratory, pulsatory, load controlled, displacement controlled?
 - What are the dynamic characteristics of the structure?
 - What factors for combined loads will result in the code intended "safety margins?"
 - What effect do the factors have on the probability of failure (sensitivity of risk)?

All these issues must be addressed to develop a rational and uniform basis for combining loads.

Basically, this approach uses the probability of a component reaching some limit state as the central parameter in the probabilistic design methodology. An example would be to

- derive partial safety factors for extreme loads (500-year quake)
- develop partial safety factors for component response to dynamic loads
- develop probabilistic design criteria for nuclear piping and equipment.

Conceptually loads are combined as shown in the following equations covering elastic working stress design (WSD) and load factors design (LFD). The examples pertain generally to piping.

Working Stress Design Criteria:

$$D + L + E + T_o + R_o < 1.35S$$

Load Factor Design Criteria:

$$0.75 (1.4 D + 1.7 L + 1.9 E + 1.7 T_o + 1.7 R_o) < U$$

where S = working stress limit

U = ultimate strength

D = dead weight stresses

L = live weight stresses

E = operating basis earthquake stresses

T_o = thermal loads

R_o = normal pipe reaction stresses

Table 12.6.2 illustrates possible factors to be used in the LFD, approach for a "loss-of-function" limit state.

Values of the various functions use the approach discussed by Chou et al. (12.6.1)

TABLE 12.6.2. Example of Format of Results of Load Combination Methodology For "loss-of-function" Limit State

Probability of Loss of Function	Loads				
	Dead Wt.	Live	E _{OBE}	Thermal	Reaction
10 ⁻²	1.0	0.6	0	0.4	0
10 ⁻³	0.9	0.8	0.5	0.6	0.3
10 ⁻⁴	1.0	1.2	1.4	1.0	0.8
10 ⁻⁵	1.5	1.8	1.7	1.8	0
10 ⁻⁵	1.6	2.0	0	2.5	0
10 ⁻⁵	1.4	1.7	1.9	1.7	1.7

Figure 12.6.5 describes the bases for the safety index β where

$$\beta = \text{safety index} = \frac{m_{R-Q}}{\sigma_{R-Q}}$$

where β is expressed in terms of the ratio of the statistical mean to the standard deviation.

This concept has considerable appeal. An obvious problem is the validation of the methodology which will require extensive peer review and comparison against the deterministic values in the present codes.

Ravindra and Singh^(12.6.2) earlier investigated the approach cited in work of reference 12.6.1 in the preceding pages. They took the specific case of a straight section of pipe (A 106 B), used the ASME III Code equations for class 2 piping (section NC) and, through use of a probabilistically based safety index, modified the deterministic Code equations into a series of

R = RESISTANCE OF COMPONENT (STRENGTH)

Q = LOAD ACTING ON COMPONENT

m = MEAN OF PDF

σ = STANDARD DEVIATION

β = SAFETY INDEX = MEASURE OF RELIABILITY

FIGURE 12.6.5. Probability Density Function Illustrating the Factors Used in Determining the Safety Index (β).

weighted probability equations. In essence, the β is that discussed in Figure 12.6.5 which is cited in reference 12.6.1 and is from the work of C. A. Cornell.

Since the handling of piping probabilistically in the major thrust of Chapter 12, the authors' (12.6.2) development is followed closely.

For purposes of evaluation, a straight section of Class 2 SA 106 B pipe, 114.3 mm in diameter with calculated wall thickness of 1.52 mm, for design pressure P_d of 5.52×10^3 kPa, and dead load moment M_A of 16.27 Nm. The allowable stress S_h for A 106 B pipe is 103.4×10^3 KPa, which is assumed to be constant over the range of temperature (38 to 343°C).

12.6.2.1 Safety Indices

The Class 2 nuclear piping is analyzed according to the requirements of NC-3650 of ASME III Code. The following equations are generally used:

$$\text{Sustained Load} \quad S_{SL} = \frac{P_d D_o}{4 t_n} + \frac{0.75 i M_A}{z} \leq 1.0 S_h \quad (12.6.1)$$

$$\text{Occasional Load} \quad S_{OL} = \frac{P_{\max} D_o}{4 t_n} + 0.75 i \left(\frac{M_A + M_B}{z} \right) \leq 1.2 S_h \quad (12.6.2)$$

$$\text{Thermal Expansion} \quad S_{TE} = \frac{P_d D_o}{4 t_n} + 0.75 i \left(\frac{M_A}{z} \right) + i \left(\frac{M_C}{z} \right) \leq (S_h + S_A) \quad (12.6.3)$$

where P_d = internal design pressure, k Pa

D_o = outside diameter of pipe, mm

t_n = nominal wall thickness, mm

M_A = resultant moment loading on cross section due to weight and other sustained loads, Nm

z = section modulus of pipe, mm³

i = stress intensification factor as given in the ASME III Code

S_h = basic material allowable stress at design temperature, k Pa

$P_{\max}$ = peak pressure, k Pa

M_B = resultant moment loading on cross section due to occasional loads such as those from flow transients and earthquake, Nm.

M_C = range of resultant moments due to thermal expansion, Nm.

S_A = allowable stress range for expansion stresses, k Pa.

12.6.2.2 Other Nomenclature

D = dead load

E = random factor representing analysis errors

F = random factor representing fabrication uncertainties

F_{eq} = earthquake load

F_y = yield strength of material

F_u = ultimate tensile strength of material

G = random factor representing uncertainties in strength calculations

M = random factor representing material strength variations

p_F = probability of failure of a component

Q = total load effect acting on a component

R_m = resistance of a component

R_n = nominal resistance

T = thermal load

T_r = transient load

V_Q = coefficient of variation of load effect

V_R = coefficient of variation of resistance

α = numerical constant = 0.55

β = safety index

σ = standard deviation of a variable

From an examination of the design requirements given by Equations (12.6.1), (12.6.2), and (12.6.3), the design variables are judged to be internal pressure P , dead load D , earthquake load F_{eq} , load from flow transients T_r , thermal load T , and material strength S . In the following, the statistical parameters of the variables listed above are estimated:

- Internal Pressure (P_d)

The internal pressure in the piping is controlled to be as near the design pressure as possible. ASME code allows a tolerance in pressure of $\pm 10\%$. Assuming that the mean internal pressure is equal to the design pressure and that the ASME tolerances are not exceeded 95% of the time, the coefficient of variation of the internal pressure is estimated as 0.05.

- Dead Load (D)

The variability of dead load is due to the differences between the actual cross sectional areas of components and their nominal areas. This variability is fairly small. The mean dead load is estimated equal to the nominal dead load and the coefficient of variation V_D as 0.05.

- Earthquake Load (F_{eq})

The uncertainty in earthquake load comes from uncertainties in the seismic history of the region, the activity rates of known geologic structures, the attenuation of ground motion from the epicenter to the site, the translation of intensity of shaking to ground acceleration, and in the dynamic properties of the system. For the purposes of consistently combining loads that vary in time, it is necessary to derive the statistics of lifetime maximum earthquake force. Using the procedure discussed in reference 12.6.1, the lifetime maximum ground acceleration for a typical nuclear plant site is estimated to have a mean of 0.03 g. The corresponding operating basis earthquake at this site is 0.10 g. The coefficient of variation of earthquake load is estimated to be 1.00.

- Flow Transient Loads (T_r)

The mean of transient loads is estimated to be equal to the calculated values using standard procedures. The coefficient of variation is taken to be 0.05.

- Thermal Loads (T)

The mean load is assumed equal to the value prescribed by the ASME code. The coefficient of variation is estimated as 0.05.

• Material Strength

The mean yield strength of the piping material, for the purposes of illustration and based on the analysis of extensive test results, is estimated as 1.05 times the specified yield strength and the coefficient of variation of yield strength as 0.10. Analysis of available test data and statistical sampling are necessary to establish these parameters for all types of piping material. The coefficients of variation of fabrication factor F and of strength properties G are estimated as 0.10 and 0.05, respectively. The coefficient of variation of resistance V_R is calculated as 0.15. Similarly, the mean of the ultimate tensile strength is 1.05 times the specified ultimate tensile strength of the material and the coefficient of variation of resistance V_R as 0.15.

12.6.2.3 Evaluation of Safety Index for a 106 B Pipe

For the purposes of illustration, a straight through pipe of outside diameter $D_o = 1143$ mm is assumed. The nominal thickness is calculated as 1.52 mm for a design pressure P_d of 5.52×10^3 kPa and a dead-load moment M_A of 16.27 Nm (Eq. 12.6.1). The piping material is SA 106 B with an allowable stress S_b equal to 103.4×10^3 kPa. For a wide range of temperatures (38°C to 343°C) the allowable stress S_b for this material is constant according to ASME Code. The stress intensification factor i is taken to be unity. In general, other values of the stress intensification factor which depend on the geometric configuration of the pipe may have to be considered to obtain representative values of the safety indices. Using the mean and the coefficient of variation of the random variables enumerated above, the value of the safety index implied in Eq. 12.6.1 is calculated as 4.7. Similar calculations show that the safety index values implied by Eqs. 12.6.2 and 12.6.3 are 4.4 and 2.8, respectively. The time variation of different types of loads are considered in evaluating the safety indices implied in these equations. For example, in Eq. 12.6.1 the load combination is one of dead load which is fairly constant in time and of lifetime maximum internal pressure. In Eq. 12.6.2, the dead load, internal pressure and flow transient load are treated as constant in time and the lifetime maximum earthquake load is combined with these loads. In Eq. 12.6.3, the thermal load which is assumed to be a lifetime maximum variable is combined with dead load and internal pressure. It may be noted that actual component

design is usually governed by Eq. 12.6.3, when considerations such as availability of sections and standard practice do not control. Hence, for deriving consistent design rules, a single value of safety index $\beta = 3.0$ may be selected. This value is chosen to be consistent with other systems such as steel building structures and concrete structures. A higher value of safety index may be chosen in view of high reliability expected of nuclear systems.

12.6.2.4 Derivation of Design Rules

With the assumed value of safety index $\beta = 3.0$, the factors to be applied on the design variables are

$$\phi = \text{strength factor} = \exp(-\alpha\beta V_R) \frac{R_m}{R_n} = 0.82$$

$$\gamma_E = \exp(\alpha\beta V_E) = 1.18$$

$$\gamma_P = 1 + \alpha\beta V_P = 1.08$$

$$\gamma_D = 1 + \alpha\beta V_D = 1.08$$

$$\gamma_{F_{eq}} = 1 + \alpha\beta V_{F_{eq}} = 2.65$$

$$\gamma_{Tr} = 1 + \alpha\beta V_{Tr} = 1.08$$

$$\gamma_T = 1 + \alpha\beta V_T = 1.08$$

Using the above factors, the three design equations corresponding to the ASME III Code Section 3650 are obtained as

$$\frac{PD_o}{4t_n} + i \left(\frac{M_A}{z} \right) \leq 0.64 F_y \quad (12.6.4)$$

$$\frac{PD_o}{4t_n} + i \left(\frac{M_A}{z} \right) + i \left(\frac{M_{Tr}}{z} \right) + 0.74 i \left(\frac{M_{eq}}{z} \right) \leq 0.64 F_y \quad (12.6.5)$$

$$\frac{PD_o}{4t_n} + i \left(\frac{M_A}{z} \right) + i \left(\frac{M_T}{z} \right) \leq 0.64 F_u \quad (12.6.6)$$

where M_{eq} is the moment due to the operating basis earthquake, and F_y and F_u are the specified yield strength and ultimate tensile strength, respectively. Equations 12.6.4, 12.6.5, and 12.6.6 have been normalized so that some load factors are unity. It may be noted that the factors on earthquake moment and on the yield and ultimate tensile strength are used because for the load combination under study the material is expected to be stressed beyond its yield strength. Equations 12.6.4, 12.6.5, and 12.6.6 all have been derived with a common value of safety index. Within the context of second-moment reliability theory, this assures consistency between these design equations.

12.6.2.5 Conclusions

The author's^(12.6.2) conclusions are included:

- Consistency between different design requirements can be achieved through reliability considerations.
- The proposed moment probabilistic method is conceptually suited to derive code equations and is relatively simple to use. It requires a knowledge of two statistical moments of the design variables, namely, mean and variance.
- The method requires the assignment of an important parameter value - a measure of reliability of a component herein called the "safety index." The safety index value may be selected as a Committee decision or may be obtained through calibration as shown in this paper.
- The implied value of safety index in the ASME Code equations are calculated to vary from 2.8 to 4.7. These values are indicative only; further studies that include a number of design situations are necessary to establish the value(s) of safety index for deriving design rules.
- For a given safety index, consistent design rules can be derived; the strength factors and load factors in these rules reflect the

uncertainties in the design variables. Loads that vary in time can be combined consistently by distinguishing between sustained and occasional loads.

- Consistency between different systems can be achieved through the use of the proposed probabilistic format.

Crack growth can be approximated by a fatigue model based on linear elastic fracture mechanics. Loads for both crack growth and possible failure can be postulated. The third parameter influencing failure will be the relevant mechanical properties. A simplistic approach would be to relate flaw size to K_{1c} ; e.g.,

$$a_{\max} = \text{const} \left(\frac{K_{1c}}{\sigma} \right)^2$$

Since the failures of interest are after pressurization and heatup, the meaningful fracture toughness properties for typical reactor piping more realistically are characteristic of elastic-plastic rather than linear-elastic properties. If we had the relevant fracture toughness data (which rarely is the case), we could evaluate

$$a_{\max} = C_o \frac{J_{1c}}{\sigma^2} \quad (12.7.1)$$

where this $a_{\max}$ should be substantially larger than the $a_{\max}$ as a function of K_{1c} .

The third possibility is a general yield model where the remaining ligament is assumed to fail by ductile tearing.

There are some experimental data relating flaw size to stress (usually flow stress) and to toughness properties; however, the data are rather limited with regard to developing a probability model inter-relating flaw size, stress and toughness properties. In terms of probability density functions, the following is my best estimate as to where the state of knowledge is:

- There is no meaningful model available for the PDF of flaw-size distribution in typical nuclear piping.

- Fracture toughness values are sensitive both to prior history and to operational history; for example, there is evidence of long-term reduction in upper shelf toughness in cast austenitics that could be significant. Degradation mechanisms exist for ferritic materials so time of operation could be significant.
- Real versus postulated transients differ widely, as do postulated faulted conditions versus the probability of their occurrence. The uncertainty in loads at any given time will be very high.

One possible approach to toughness properties would be to develop a statistical distribution of available data, recognizing the existence of outliers such as discussed by Öfverbeck and Östberg.^(12.7.1)

The failure load-temperature relationship is illustrated schematically in Figure 12.7.1.

FIGURE 12.7.1. Failure Load-Temperature Relationship Schematically Displayed Illustrating Various Toughness Regions for a Typical Primary Pipe

12.7.1 Failure Models

Several models have been suggested for evaluation of the failure characteristics of materials or components. The LEFM (K_{Ic}) approach is generally accepted for handling failures in the elastic regime. Several options exist for EPFM such as the J-contour integral, crack-opening displacement (COD), equivalent energy, and K corrected for the plastic-zone size. The third possibility, typified by general yielding and failure of the ligament by ductile tearing, can be handled with the tearing modulus. Another approach, bridging from brittle to ductile fracture, is the Failure Assessment Diagram (FAD) developed by CEEB.

In addressing the applicability of any model it is appropriate to ask the following as a minimum:

- Is fatigue crack growth or growth by mechanisms such as IGSCC sensitive to the characteristics of a particular model?
- How sensitive is a model to the conditions of material toughness and degree of constraint?
- How accurately does any model predict the actual failure behavior under a given set of conditions and over a range of conditions? In this context, how well do models compare for a specific set of failure conditions?
- How well can the model be applied to a real structure for accurately predicting failure behavior?

Fatigue crack growth is not particularly sensitive to the various models. Rather, such growth is controlled by environmental conditions, and the characteristics of the stress cycles. Generally, we can decouple the crack-growth stage from the failure mode. The same is true for crack-growth mechanisms such as IGSCC.

Harrison^(12.7.2) examined the uncorrected maximum load plus four elastoplastic fracture mechanics parameters; i.e., J-contour integral, equivalent energy (K^*), K corrected for plastic-zone size (K'), and crack-opening displacement (COD). The first three were found to be roughly equivalent and all

were directly related to COD. This was particularly true for lower strength weld metals. These comparisons were based on test specimens meeting the validity criteria for the various parameters. Specifically, the deeply notched bend bar was used.

The figures from the Harrison report^(12.7.2) indicate the correlations. The approach was to plot nondimensional ratios representing the stress intensity factor for maximum load, $\frac{1}{a} \left(\frac{K_m}{\sigma_y} \right)^2$; the stress intensity factor corrected for plastic zone size, $\frac{1}{a} \left(\frac{K'}{\sigma_y} \right)^2$; the equivalent energy, $\frac{1}{a} \left(\frac{K^*}{\sigma_y} \right)^2$; and the J-contour integral, $\frac{1}{a} \left(\frac{J_E}{\sigma_y} \right)$; against the ratio representing COD, $\frac{1}{a} \left(\frac{\delta}{e_y} \right)$.

Figure 12.7.2 relates the stress intensity factor for maximum load to COD. The data points primarily lie between the computed plane strain and plane stress solutions. As expected the points lie closer to the plane stress solution at high strains, while they are closer the plane strain solution for

FIGURE 12.7.2. Relationship Between Stress Intensity Factor for Maximum Load and COD

$\frac{1}{a} \left(\frac{\delta}{e_y} \right) = 1$ and below. The collapse load-line assuming a nonwork hardening material underpredicts markedly, indicating that work hardening must be considered.

If the data in Figure 12.7.2 are corrected for plastic-zone size, one obtains plots such as in Figure 12.7.3. Here the straight lines utilize a plastic-zone correction based on the method of Allen discussed in Reference 12.7.2. As noted, the various empirical corrections deviate markedly from the linear relationship at higher strains.

Figures 12.7.4 and 12.7.5 relate equivalent energy, (K^*) , and J-contour integral to COD. Again, a fair to good equivalence is noted.

FIGURE 12.7.3. Relationship Between Stress Intensity Factor Corrected for Plastic-Zone Size K' and COD

FIGURE 12.7.4. Relationship Between Equivalent Energy K and COD

The correlations range from quite good to fair. Certainly, over portions of the stress-strain range, the various parameters are quite comparable when one uses data from a specific type of test bar.

A more important question has to do with the prediction of behavior in real structures. While the correlations may appear to be quite good for a specific type of test specimen, the use of a given parameter to predict response in a real structure may be both difficult and fraught with error. Egan and Robinson^(12.7.3) argue that although COD and J-contour integral are equivalent, COD has several advantages; for example, the handling of secondary stresses such as residuals. Since COD is a physical measure of crack-tip conditions, including stress state and thickness, it is more relevant to real structures. The authors^(12.7.3) argue that the J-contour integral is appropriate for in-plane problems but not for real structures.

FIGURE 12.7.5. Relationship Between J-Contour Integral and COD

If the material toughness and acting stresses or strains are known, COD can be used to estimate the significance of flaws of various sizes in structures.

Figure 12.7.2 illustrates that LEFM may remain valid well above the validity ratios for plane strain. In thicker sections, the plastic-zone size may be contained so that even with a large plastic-zone $r_y \sim B$, large (general) strains will not develop, and the elastic assumptions remain reasonably accurate.

In piping, EPFM is considered to better represent the structural response where the three parameters—flaw size, fracture toughness and acting stress—will characterize the structural response. In many structures, such as pressure vessels and piping, the design has adequate toughness in highly stressed

regions where extensive plasticity may occur. In such instances, cracks may initiate and grow; however, there is a high probability of detection prior to failure.

The authors^(12.7.3) reviewed the strengths and weaknesses of the various models used to handle behavior of yielded structures, including the following:

1. Gross Strain Crack Tolerance - It is not suggested because it is dependent not only on material properties, but also on crack size, specimen size, specimen shape, and strain gradients.
2. J-Contour Integral - Although there has been extensive work on it no general method exists for predicting the flaw size expected to cause failure in terms of known stresses ($\sigma > \sigma_y$) and actual values of J_{Ia} . This is due to the complex stress-strain distributions in real structures such as nozzles where a 3-D analysis is needed, and, to date, no 3-D J-concept exists.
3. Equivalent Energy - It suffers from some of the same limitations as the J-contour integral as temperature increases. K_{Icd} tends to overestimate K_{Ic} .
4. Crack Opening Displacement - The authors argue that COD can be derived from a linear analyses and extended to the nonlinear regime. The Irwin crack repositioning can be used to handle crack-tip plasticity.

The following is a general defect tolerance equation applicable to real structures related COD, design stress (σ_d) and flaw size:

$$\delta = C_2 e_y a \quad \text{for} \quad \begin{bmatrix} \sigma_d = f(\sigma_y) \\ \sigma_d < \sigma_y \end{bmatrix} \quad (12.7.2)$$

where C_2 is a constant.

To avoid failure, $\delta_a > \delta = C_2 e_y a$, or

$$a_{tol} < C_2 (\delta_a / e_y)$$

where e_y is strain at yield = σ_y / E .

Table 12.7.1^(12.7.3) illustrates the sensitivity to the model selected with regard to accuracy of prediction.

An example of EPFM, applied to the prediction of the failure behavior of a feedwater nozzle, was used by the authors^(12.7.3) to illustrate the applicability of COD to real structures as well as the limitations of the J-contour integral. Data from the HSST-ITV-6 were used. Values used were as follows:

$C_2 = f(e) = 0.14$ for stress relieved vessel

Stress concentration factor = 2.8

Allowable hoop stress $\sigma_h = 25$ ksi

TABLE 12.7.1. Summary of Flaw-Size Prediction

Total stress environment $\sigma < \sigma_y$		
Accuracy of predictions ↓ Decreasing	a) Full K or COD analysis available	Use $a_{max.} = \text{Const } (K_{1C} / \sigma_y)^2 = \text{Const } (\delta_c / e_y)$ where constant includes (i) Flaw shape (ii) Effect of finite boundaries (iii) Effect of geometry (iv) σ / σ_y ratio
	b) Full K or COD analysis not available	Approximate detail to a geometry for which K or COD analysis is available then use $a_{max.} = \text{Const } (K_{1C} / \sigma_y)^2 = \text{Const } (\delta / e_y)$ where constant may include (i) - (iv) above
	c) No K or COD analysis available	Use $a_{max} = \text{Const } (K_{1C} / \sigma_y)^2 = \text{Const } (\delta / e_y)$ where constant includes only (iv) above, i.e., use infinite plate case.

Total acting stress at nozzle = $2.8 \times 25 = 70$ ksi

e/e_y at $\sigma_y = 50$ ksi = 1.4

$a_{tol} = 3.6$ inch

a_{tol} = depth of surface flaw or half length of through-thickness flaw

Effect of σ_{res} - depends on local strain level ranging from factor of 2 to 5

The COD handles σ_{res} which becomes quite important the closer fracture comes to LEFM. Under these circumstances, the J-contour integral becomes increasingly less adequate because J measurements are calculated for global loads and deflections, and such loads and deflections are not affected by equilibrium secondary stresses such as residuals.

This same COD approach can be used to establish whether a material has sufficient toughness to support a crack prior to fracture. Figure 12.7.6 is a nomogram permitting the calculation of the critical COD for leak-before-break.

Based on analysis of HSST ITV tests, the authors argue that fracture initiation parameters, such as K_{Ic} , K_{Icd} , COD, J_{Ic} , are inadequate to predict full-scale structural behavior where appreciable ductile tearing precedes fracture. The accuracy of the COD analysis depends on relationships between COD and applied strain where COD has the advantage of characterizing the crack-tip strain environment, and taking into account both strain concentrations and residual stresses.

12.7.1.1 Tearing Modulus

A tearing modulus stability criterion has been used to perform a simplified fracture mechanics analysis on BWR stainless steel piping which had undergone severe intergranular stress corrosion cracking (IGSCC).^(12.7.4) The tearing stability concept permitted an evaluation of the probability of unstable crack extension. The authors^(12.7.4) concluded that such unstable crack extension (leak-to-failure) was highly improbable in BWR stainless steel piping systems designed to ASME Section III even after severe IGSCC. Unstable fracture was postulated only when piping length to radius ratios (L/R) were very large

FIGURE 12.7.6. Nomogram for Critical COD for Leak-Before-Break

(~200). Since most values of L/R are more nearly 20, there are large margins against unstable fracture. This assumes bending loads control.

It is necessary to evaluate two constants:

1. T_{mat} = nondimensional materials constant,

$$T_{mat} = E/\sigma_o^2 \frac{dJ_{mat}}{da} \quad (12.7.3)$$

2. T_{appl} = nondimensional applied constant,

$$T_{appl} = \frac{E}{\sigma_o^2} \frac{dJ}{da} \quad (12.7.4)$$

$$\text{Stable} = T_{mat} > T_{appl}$$

$$\text{Unstable} = T_{mat} < T_{appl}$$

12.7.1.2. Failure Assessment Diagram

The Failure Assessment Diagram (FAD) represents a potentially powerful tool for predicting the failure behavior of structures^(12.7.5,12.7.6,12.7.7) under both ductile and brittle conditions. The original report^(12.7.5) was developed to be applied to ferritic steel structures, primarily reactor pressure vessels. The purpose of the FAD was to remove some of the conservatism of the LEFM approach. The FAD two-criteria approach consists of the following:

1. A region where failure is controlled by the crack-tip properties (LEFM).
2. A region where failure is controlled by the properties of the ligament (plastic limit theory).

Figure 12.7.7 represents a typical Failure Assessment Diagram. The axes are

$$K_r = \frac{K_1}{K_{1c}} = \frac{\text{load (stresses) being considered}}{\text{load (stress) to cause linear elastic failure}} \quad (12.7.5)$$

$$K_r = S_r \left\{ \frac{8}{\pi^2} \ln \sec \left(\frac{\pi}{2} S_r \right) \right\}^{-1/2}$$

$$S_r = \frac{P_f}{P_{UC}(1-a/t)}; \quad K_r = \frac{1.6 \sigma_m \sqrt{\pi a}}{K_{IC}}$$

FIGURE 12.7.7. The Failure Assessment Diagram

$$S_r = \frac{\sigma_a}{\sigma_1} = \frac{\text{load (stresses) being considered}}{\text{plastic limit load (stress) in the structure}} \quad (12.7.6)$$

The original assessment^(12.7.5) was quite restricted; as cited previously, it was directed primarily to ferritic pressure vessels. It did not consider the following:

- the arrest of a running crack such as could occur with high-thermal and low-pressure stresses
- vessels operating in the creep regime
- stress corrosion cracking

- methods of stress analysis, although some finite element analyses were used
- stresses due to impact loads.

Subsequent reports considered residual and thermal stresses^(12.7.6) and piping systems.^(12.7.7)

The initial report^(12.7.5) presents a step-by-step procedure for establishing the acceptability or rejectability of a flaw in a structure. Basically the approach is quite similar to that in Appendix A of ASME Section XI. For example:

1. define flaw as to shape and orientation
2. define the relevant loads (stresses)
3. determine the lower-bound mechanical properties
4. determine the structure collapse load
5. determine K_I
6. determine whether flaw is critical using each loading condition
7. if acceptable, determine flaw growth to end-of-life
8. repeat above analysis for end-of-life flaw.

The limit load approach to establishing structural collapse may lack sufficient accuracy for larger flaws, based on an ASME XI analysis. Other questions concerning the model pertain to combining primary and secondary stresses, and to selecting the correct toughness values for slow crack growth and failure.

With regard to the limit load approach for ductile ligament failure, the authors^(12.7.5) suggest the following approaches:

1. Use the procedures in Appendix 4.2 of their report.
2. If this is not acceptable, perform a 3-D elastic-plastic finite element analysis.
3. Another alternative is to test models to failure.

Chell^(12.7.6) extended the original model to more realistically incorporate thermal and residual stresses into the FAD. The author rederived and reinterpreted the FAD in terms of an equivalent J-integral analysis. The J

values indicated that the FAD is a good approximation of a lower-bound failure criterion for mechanical loading, and this approach permits inclusion of thermal residual and secondary stresses through a point, transformed from a failure diagram unique to a given system to the general FAD. The J approach is valuable where a plastic collapse parameter is not definable; however, the secondary stresses tend to reduce the effective failure loads in the post-yield regime.

The equation defining the FAD is given in Figure 12.7.7 and is repeated here:

$$K_r = S_r \left[\frac{8}{\pi} \ln \sec \left(\frac{\pi}{2} S_r \right) \right]^{-1/2} \quad (12.7.7)$$

The same equation can be used for the elastic-plastic case where K_r is defined as

$$K_r = \frac{K_I}{K_p} = \left(\frac{J_I}{J} \right)^{1/2} \quad (12.7.8)$$

and K_p is equivalent to K_c .

Chell's paper^(12.7.6) develops the procedures for handling thermal and residual stresses in a step-by-step fashion that is relatively easy to follow. If the elastic-plastic stress intensity factor, K_p , has been evaluated, which is rarely the case, a direct graphical solution is possible. Otherwise, the approach is to pessimistically evaluate K_p . The FAD can be described by

$$K_r^f = F(S_r^f)$$

where $F(S_r)$ is the right-hand side of the $\ln \sec$ equation. An equation giving 96% accuracy is

$$S_r^f = \left(1 - \exp \left\{ \frac{-24}{\pi^2} \left[\frac{1}{(K_r^f)^2} - 1 \right] \right\} \right)^{-1/2} \quad (12.7.9)$$

Since the FAD concept relies on elastic or fully plastic solutions, K_p must be specified based solely on elastic analyses, using techniques such as Irwin's first-order plasticity correction; however, this approach may not apply for thermal or residual stresses in the absence of mechanical stresses.

Darlaston^(12.7.7) extended the FAD concept to piping as well as developing the bases of the failure assessment, two-criteria approach more completely. Figure 12.7.8 illustrates the two regimes; namely, 1) failure crack-tip controlled and 2) collapse controlled.

FIGURE 12.7.8. Failure Assessment, Two-Criteria Approach

The FAD has been validated for both pressure vessels and piping. Figure 12.7.9(a) covers the pressure vessel case and Figure 12.7.9(b) examines a number of tests on piping for leak-before-break and for failure.

The author^(12.7.7) emphasizes that safety factors are added after analyses so that they can be based on the significance of failure of a given component.

Examples of the use of the FAD for failure by three different mechanisms are given in Figures 12.7.10(a), (b), (c). In the first case, failure occurs by an increase in internal pressure denoted by an extension of a line through

FIGURE 12.7.9(a). Validation of Application of Failure Assessment Diagram

FIGURE 12.7.9(b). Failure Assessment Diagram of the Leak/Break Results

the origin. The second case is failure due to inadequate fracture toughness. The third instance is an assessment of the critical size of crack to cause failure.

The Marshall Report^(12.2.4) developed continuous distributions for fracture toughness (K_{1c}). These could be modified by substitution of various elastic-plastic parameters relevant to piping. Examples of K_{1c} equations follow:

$$f(K_{1c}) = \frac{1}{(2\pi)^{1/2}\alpha} \exp - \left[\frac{(K_{1c} - \bar{K}_{1c})^2}{2\alpha^2} \right] \quad (12.7.10)$$

where α is the standard deviation.

$$S_r = \frac{P_f}{P_{uc}(1-a_t)} \cdot K_r \cdot \frac{1.6 \sigma_m \sqrt{\pi a}}{K_{Ic}}$$

FIGURE 12.7.10. Examples of the Application of the Failure Assessment Diagram to Variations in (a) Internal Pressure; (b) Fracture Toughness; (c) Crack Depth

The preceding is a normal distribution. A Weibull distribution can be used:

$$f(K_{1c}) = K (K_{1c} - K_0)^m \exp \left[-\frac{K}{(m+1)} (K_{1c} - K_0)^{m+1} \right] \quad (12.7.11)$$

where

$$f(K_{1c}) = 0 \quad \text{If } K_{1c} < K_0$$

The most probable value of K_{1c} is

$$\bar{K}_{1c} = K_0 + \left(\frac{m}{K} \right)^{\frac{1}{m+1}} \quad (12.7.12)$$

and the standard deviation is

$$\alpha = \left(\frac{m+1}{K} \right)^{\frac{1}{m+1}} \left[\frac{1}{\Gamma} \left(\frac{m+3}{m+1} \right) - \left[\Gamma^2 \left(\frac{m+2}{m+1} \right) \right]^{1/2} \right]$$

where Γ is the usual gamma-function; K_0 is a lower bound cutoff value, and m and k are parameters.

At this stage, we should have all the necessary inputs to permit the prediction of failure. Given a system or subsystem made up of components having a defined range of mechanical properties and operating under a defined set of temperatures and loads, the problem is to determine the probability of failure if one or more of the components contain a spectrum of flaws. Basically the solution is one of load versus strength with the flaws decreasing the effective strength as noted in Figure 12.1.1. We can define loads in terms of stress, stress intensity factor or J , and strength in terms of strength, K_{1c} , or J_{1c} .

The most extensive developments of the failure probability equations are those of Lidiard and Williams^(12.2.3) and Harris.^(12.2.6) These will be used for the overall description, supplemented by the work of others where it is desirable to expand the scope.

The Lidiard and Williams report^(12.2.3) is an extension of an earlier analysis in the Marshall Report.^(12.2.4) Both expand on the early work of Becher and Pederson.^(12.1.2) Interest in these documents was in the failure of pressure vessels, so linear elastic fracture mechanics (LEFM) was used. In a large population of similar pressure vessels, it is reasonable to assume that there will be a statistical distribution of cracks; their depths, shapes and locations, fracture toughness, K_{1c} , (or other relevant mechanical properties); and loads such as the transients. Three quantities must be defined by appropriate equations:

1. The crack distribution in terms of shape, size, location and orientation. This distribution may be modified by NDE and subsequent repair as well as by growth through mechanisms such as fatigue.
2. The statistical distribution of strength. For vessels, this will be the upper-shelf fracture toughness such as $f(K_{1c})dK_{1c}$, which will give the probability that the toughness in any vessel in any location lies between K_{1c} and $K_{1c} + dK_{1c}$ which can be normalized to this equation:

$$\int_0^{\infty} f(K_{1c})dK_{1c} = 1$$

recognizing that the values of K will vary with time, so we are considering a three-dimensional diagram.

3. The design transients for normal, upset and test conditions (which can be extended to emergency and faulted). These transients are assumed to occur regularly in time at the design frequency. They will influence both flaw growth and ultimate failure probability.

In a population of vessels (or other components), the incidence of cracking can be specified by a distribution, $N(X)$, in crack depth cited previously in Section 12.3. More realistically, partial crack distributions, $N^{(r)}(X)$, for each region (N, r) of a component can be considered. The usual method is to assume the cracks are uniformly distributed throughout the vessel structure so $N^{(r)}(X)$ is simply $N(X)$ multiplied by the fractional volume of the region r. A more complicated distribution can be handled where the flaws are assumed to be distributed preferentially. With either approach, $N^{(r)}(X)$ will depend on time to account for slow crack growth. For the fraction of vessel (or other component) population having material properties, such that the critical crack depth in region r lies between a and a + da under the application of a stress system σ , then $P^{(r)}(a) da$ will represent this fraction of population; and, under the assumed failure criterion, all systems for which $X > a$ in the same region will fail under the application of σ . Therefore, the fraction of the population to fail under σ is a summation over all regions of all such possibilities such as the following:^(12.2.4)

$$P_f = P_f^{(r)} = \sum_r \int_0^h N^{(r)}(X) dX \int_0^X P^{(r)}(a) da \quad (12.8.1(a))$$

$$P_f = \sum_r \int_0^h P^{(r)}(a) da \int_a^h N^{(r)}(x) dx \quad (12.8.1(b))$$

where h is component thickness and P_f is a failure integral.

Since it is assumed that all the vessels in the population suffer the same stresses under the same transient, a given region of the vessel will bear a 1:1 relation between critical crack depth and K_{1c} so that $P^{(r)}(a) da$ can be replaced by $f(K_{1c}) dK_{1c}$ so that the preceding equation becomes^(12.2.4)

$$P_f = \sum \int_0^h f(K_{1c}) dK_{1c} \int_a^h N^{(r)}(x) dx \quad (12.8.2)$$

$$= \Sigma P_f^{(r)}$$

The lower limit a is related to K_{1c} by

$$K_{1c} = K_I^{(r)}(\sigma, a)$$

where $K_I^{(r)}$ is the stress-intensity function for the stress system, σ , and the crack location cited.

In the preceding analysis, the failure integral, P_f , is not the failure probability per vessel-year which is usually wanted. P_f is the fraction of the original population which fails when σ is applied at time t , given that neither σ nor any more severe system of stresses has been applied previously. The preceding is an important qualification. The first application of σ fails that fraction of the vessel population and subsequent applications of σ should generate no further failures in a reasonable time frame.

Figure 12.8.1 schematically represents the area of integration and the density of the contributions of P_f for a particular region of a vessel.

The cold hydro becomes an important case because the probability equations will predict a finite fraction of failures under hydro loads. Indeed past history bears this out. However, if the hydro temperature is high enough, the fraction predicted to fail becomes a very small number. Obviously, if the critical crack sizes for the pre-service pressure test are smaller than those

FIGURE 12.8.1. Area of Integration and Density of Contribution to the Integral $P_f(r)$

for all other transients subsequently encountered, the failure probability would be zero. If the properties change with time, this will not remain the case. The obvious change will be in flaw size since the existing flaws will tend to grow with time under the load transients. This change in size with time can be defined as

$$x_0 = \xi^{-1}(x_1, t) \quad (12.8.3)$$

If we define the failure probability as Q_f where

$$Q_f = \frac{dP_f}{dt} \quad (12.8.4)$$

we can rewrite the general equation for normal, upset and test conditions as

$$Q_f^N = \sum_r \int_{B(r)} f(K_{1c}) N(r) \left[(\xi^{-1}(a)) \right] \frac{\partial \xi^{-1}}{\partial t}(a) dK_{1c} \quad (12.8.5)$$

The same form of equation is used for emergency and faulted conditions where

$$Q_f^E = f_E P_f(\underline{\sigma}^E, t) \quad (12.1.3)$$

The toughness properties usually have been assumed to fit a Gaussian (normal) distribution so the probability $\bar{K}_{1c}$ that lies in the interval K_{1c} to $K_{1c} + dK_{1c}$ will take the form

$$f(K_{1c}) dK_{1c} = \frac{1}{(2\pi)^{1/2} \alpha} \exp - \left[\frac{(K_{1c} - \bar{K}_{1c})^2}{2\alpha^2} \right] dK_{1c} \quad (12.7.10)$$

where α is the standard deviation and K_{1c} is the most probable toughness.

Alternately, a Weibull distribution will take the form

$$f(K_{1c}) = K(K_{1c} - K_0)^m \exp \left(- \frac{K}{(m+1)} (K_{1c} - K_0)^{m+1} \right) \quad (12.7.11)$$

for $K_{1c} > K_0$. As cited previously the crack-detection distribution will be

$$N(X) = A(X) B(X) \quad (12.3.1(a))$$

$$\text{where } A(X) = A \exp(-\lambda X) \quad (12.2.2(a))$$

$$B(X) = \epsilon + (1-\epsilon) \exp(-\mu X) \quad (12.3.2(a))$$

The solution of the preceding equations will yield failure probabilities as functions of years of operation. Figure 12.8.2 is one such solution where a Weibull distribution of toughness ($\bar{K}_{1C} = 220 \text{ ksi (in.)}^{1/2}$, $\alpha = 22 \text{ ksi (in.)}^{1/2}$) with a crack growth coefficient $\gamma = 5 \times 10^{-3}$.

Harris^(12.2.6) used different terminology in developing equations similar to Lidiard and Williams.^(12.2.3) Conditional probabilities and error function notation were used. The conditional crack depth distribution corresponding to a log-normal fit of the data was developed in Section 12.2 equations (12.2.4 - 12.2.7). Equation 12.2.7(b) is the end product:

$$\frac{P(>a)}{p^*} = \frac{1}{2} \operatorname{erfc} \left(\frac{1}{\mu^{1/2}} \ln a/\lambda \right) \quad (12.2.7(b))$$

FIGURE 12.8.2. Calculated Time Dependence of the Total Failure Probability. Q_N , per PWR Vessel-year for Normal, Upset and Test Conditions as Obtained with a Weibull Distribution of Toughness ($\bar{K}_{1C} = 220$ and $\alpha = 22 \text{ ksi in.}^{1/2}$) and a Crack Growth Coefficient $\alpha = 5 \times 10^{-5}$, Plotted as $\text{Log}_{10} Q_f$.

Harris^(12.2.6) combined the probability of nondetection of flaws (12.3.3) with the probability of existence (e.g., 12.2.7(b)) to obtain a flaw population after nondestructive examination and repair. This equation is

$$P(>a) = p^* \frac{1}{2\mu\nu (2\pi)^{1/2}} \int_a^\infty \exp - \frac{1}{2\mu^2} (\ln x/\lambda)^2 \operatorname{erfc}(\nu \ln x/a^*) \frac{dx}{x} \quad (12.8.6)$$

Since the major interest in failure usually is in terms of time, failure probability can be expressed in terms of per-unit time through use of hazard functions:

$$\text{Hazard function} = \frac{dP_f/dt}{1 - P_f} \quad (12.8.7(a))$$

Most failure probabilities in nuclear systems are quite low so $P_f \ll 1$; therefore, the hazard function can be simplified to

$$\text{Hazard function} \cong P_f = dP_f/dt \quad (12.8.7(b))$$

Combining (12.8.7(b)) with (12.8.6), and modifying terms, yields

$$P_f = \frac{dP_f}{dt} = \frac{dP_f}{da_{tol}} \cdot \frac{da_{tol}}{dt} = - \frac{da_{tol}}{dt} \frac{p^*}{2\mu a_{tol} (2\pi)^{1/2}} \exp - \frac{1}{2\mu^2} \left(\ln \frac{a_{tol}}{\lambda} \right)^2 \operatorname{erfc} \left(\nu \ln \frac{a_{tol}}{a^*} \right) \quad (12.8.8)$$

The final step is the calculation of failure probabilities. The governing equations, covering the statistical distribution of failure rate of given components can be written as

$$P_f = \frac{C^2 \pi h^2 S_{40} f(a_{tol})^4 p^*}{80\mu a_{tol} (2\pi)^{1/2}} \exp - \frac{1}{2\mu^2} \left(\ln \frac{a_{tol}}{\lambda} \right)^2 \operatorname{erfc} \left(\sqrt{\nu} \ln \frac{a_{tol}}{a^*} \right) \quad (12.8.9)$$

Specific values of the various constants are given by Harris^(12.2.6) for this case which was applied to the failure of a reactor pressure vessel nozzle-pipe region followed by pressurization of the cavity. However, it can be used for failure in any location. Figures 12.8.3 and 12.8.4 illustrate the effect of various distributed parameters on failure probability.

FIGURE 12.8.3. Various Percentiles of Probability of Failure (p_f) as a Function of Time Along With Corresponding Deterministic Results. All parameters except p_v^* distributed.

FIGURE 12.8.4. Various Percentiles of Probability of Failure (p_f) as a Function of Time Along with Corresponding Deterministic Results. C and σ only distributed parameters.

Harrop and Lidiard^(12.8.1) modified the original approach of Lidiard and Williams^(12.2.3, 12.2.4) utilizing LEFM to the case where plastic deformation is assumed to occur. The new model introduced Irwin's plastic-zone correction to better handle the zone of plastic deformation around the crack tip:

$$a_e = a + r$$

where a is actual crack tip, a_e is effective crack size, and r is plastic-zone radius:

$$r = \frac{1}{c} \left(\frac{K_I}{\sigma_y} \right)^2 \quad (12.8.10)$$

A second change consisted of the use of improved stress intensity functions, either the semi-elliptic crack of Appendix A of ASME Section XI, or a better defined model for line cracks.

Finally, a simple plastic-collapse model was introduced where the limiting crack depth, a_c , is given by

$$\frac{a_c}{h} = 1 - \left(\frac{\sigma_m + \sigma_b/2}{\sigma} \right)^2 \quad (12.8.11)$$

where yielding is assumed to occur as σ_u .

Fox and Hill^(12.8.2) developed a somewhat similar probability model. The probability of failure, (P), of a component containing a defect, (a), is given by

$$P = \int_{K_{Imin}}^{K_{Imax}} P_1(K_I) \left\{ \int_0^{K_I} P_2(K_{Ic}) dK_I \right\} \quad (12.8.12)$$

where $P_1 K_I$ is the probability density function for stress intensity, and $P_2(K_{Ic})$ is the PDF for fracture toughness.

For LEFM, $P_1 K_I$ can be defined by

$$P_1 K_I = f(G, P_3(\sigma), \pi^{1/2} a)$$

where P_3 is the PDF for applied stress. G is a factor dependent on defect shape and local geometry. For the case of post-yield fracture mechanics (PYFM),

$$K_I = \sigma_u G \left\{ \frac{8a}{\pi} \ln \sec \left(\frac{\sigma \pi}{2\sigma_u} \right) \right\} \quad (12.8.13)$$

where σ_u is ultimate tensile strength.

Note the preceding equation is of the form used in the FAD. (12.7.6, 12.7.7)

The authors^(12.8.2) warn that the standard rules for the formulation of PDF's must be followed if one is to construct a meaningful $P_I K_I$ from the preceding equations.

A PDF for applied stress--typically that of the general membrane region of a nuclear pressure vessel -- will be of the form

$$P_3(\sigma) = \frac{A}{\sigma^{22}} \exp \left(-\frac{B}{\sigma^{21}} \right) \quad (12.8.14)$$

where A and B are constants.

The distribution of fracture toughness was assumed to be Gaussian, or

$$P_2(K_{1c}) = \frac{1}{2^{1/2} \pi \alpha} \exp \left[-\frac{(K_{1c} - \bar{K}_{1c})^2}{2\alpha^2} \right] \quad (12.8.15)$$

where α = std dev.

The depth of a given crack of initial depth, a_0 , can be determined at time, (T), by

$$a(T) = a_0 (1 - \nu a_0^\nu \gamma T)^{-1/\nu} \quad (12.8.16)$$

The authors^(12.8.2) considered the application of their approach to the FAD. (12.7.5, 12.7.6, 12.7.7) They suggest an equation such as the following to handle values of K_r and S_r :

$$P_F = \int_A P_A(S_r, K_r) \left\{ \int_B (L_F) dL_F dA \right\} \quad (12.8.17)$$

where $P_A(S_r, K_r)$ is the PDF for probability of occurrence of a point anywhere on the FAD. The outer integral of the above equation is performed over the area A on the FAD between the limits of both K_r and K_S . $P_B(L_F)$ is the PDF for the probability of a point anywhere on the failure locus. The authors^(12.8.2) are preparing a computer code for the above equation.

The probabilistic approach covered previously in this section assumes the existence or initiation of a defect which then grows by fatigue due to the spectrum of loads. Another approach is advanced by Thomas^(12.8.3) who argues that neither theory nor fact justifies the assumption that the progress of an individual crack somehow represents the overall failure probability of a vessel. Since his approach differs diametrically from most others, his conclusions and a brief summary of his model are included here.

12.8.1 Conclusions and Recommendations On Age Factors

1. Failure rates for pressure vessels generally improve with age. Several samples of real-life failure data show this clearly.
2. Three broad categories of time-related improvements in failure rates may be identified. These are long-term changes in a technology, shorter term design learning curves, and typical bathtub curves. These bear a hierarchical relationship to one another.
3. The front end of the bathtub curves for pressure vessels spans 20 to 30 years at least.
4. The principal component of change to be found is the bathtub one. The other components have a lesser influence on the observed statistics for failure with age.
5. Statistics for very varied populations may be compared for age effect by reducing them to a nondimensional ratio based on the total failure number in the first 10 years.
6. In their nondimensional form all the different age effect curves have a striking similarity. An arithmetic average curve is numerically very close to each individual curve; and may be taken to be generally representative.

7. The factor F may be taken as this average for all the samples available in spite of the disturbing influences of the other factors and biases to be expected in general statistics. Sensitivity analyses show these other influences to be marginal in effect. Factor F is therefore determined with a reasonable accuracy.
8. Factor B , on the other hand, may only be roughly estimated from the data available. A hypothetical curve is offered for consideration, but it should only be used with caution.
9. It may be shown that the general curve for F applies also to serious defects and to ruptures, so that the approximation strategy suggested in Figure 12.8.5 is valid in that respect.
10. Unless one is estimating for a new plant design there is no need to consider factor B . It may be assumed to be 1. New designs may be penalized by an appropriate curve.
11. Many theoretical models in the literature are in conflict with observed facts in the relationship between age and failure probability. They are all based on fatigue and crack growth. They all assume that the progress of an individual crack somehow represents the overall failure probability of a vessel. This assumption is not justified by any theory or fact.

12.8.2 Basis for Model

The author^(12.8.3) uses the following two probabilities as a basis for his model:

The probability of a small leakage failure P_L and the probability of rupture P_C are both required separately because they have different safety implications. P_L is a highly detectable warning of danger; P_C , on the other hand, may be the start of an accident chain. His model has the following attributes:

- P_L and P_C are separately identified and the model applies to discrete parts and features of a plant.

FIGURE 12.8.5. The Overall Approximation Strategy for Vessel or Piping Failure

- It recognizes the relative importance of the various factors involved and a first approximation is based on the most significant factor. The input data required for this is readily available, and all the calculations are easily and rapidly made by hand.
- The approach is based on observed service failure statistics and recognizes the multiplicity of failure causes and modes. The first approximation does not require the analysis of any particular one, such as fatigue.

- Each factor which is modeled is scaled with a dimensionless ratio or group and each is separately amenable to statistical validation.
- The whole approach, being modular and flexible, is capable of continuous and piecemeal improvement and development. Estimates based on it may be readily updated with the growing wealth of statistical data.

The approach makes it possible to use all the statistical information which is available about various aspects of failure probability. It also provides a broad and comprehensive framework which can incorporate more detailed but incomplete models.

12.8.3 Approximation Strategy

His overall attack is given in Figure 12.8.5 which shows a broad approximation strategy envisaged in order to approach the general problem of estimating vessel and component failure probabilities. The probability of catastrophic leakage P_C is considered to be a subset of the more general leakage probability P_L ; and since it is possible to estimate P_C/P_L , then P_C may be determined, given an estimate for P_L . There are many possible approaches to estimating P_L , including the direct observation of statistics for service failures.

Figure 12.8.5 shows an approach for P_L which first identifies a global estimate based simply on size, shape and weldment factors (Q_C) and modified by the influence of age (F). These factors may relate a component to some large known data base, Q_C being the most powerful factor of all. This global estimate may then be modified to specific plant factors including the influence (B) of learning curves for a given technology and design; and any failure risk improvement factors due to quality. More precise estimates then become feasible by modeling the influence of all the known failure modes such as fatigue, stress corrosion cracking, etc. The modified global estimate may be factored up or down according to the sum effect of all the factors for the various failure causes.

P_C/P_L may be estimated by using some statistics for actual rupture cases, and by using a fracture mechanics model. The overall estimate for P_C/P_L is the sum of several categories of rupture causes which may be identified.

One other factor is introduced. This is a quantifier Q which evaluates the change in risk due to size and shape differences. For weld zones, a penalty factor of 50 Q is used. A scaling factor F is applied to correct for plant age. This is an inverse exponential related to plant age. The fourth factor is the average or global leakage failure rate for a typical plant.

The first three factors measure the opportunity to fail while the fourth yields a failure rate.

Q is defined as a dimensionless quantity,

$$Q = DL t^{-2}$$

where D = diameter of component

L = length

t^{-2} = inverse square of wall thickness.

On the basis that leakage failure rates are determinable

$$P_L \sim \left(\begin{array}{c} \text{Rate Factor} \\ \sim 10^{-7} \end{array} \right) Q_C F$$

The author^(12.8.3) examines several classes of failure data for both piping and pressure vessels to validate his model. These are presented in the paper and the interested reader is referred to the paper. The contention enunciated in the conclusions that the theoretical models do not mirror actual experience is of particular interest. Figure 12.8.6 from the paper illustrates the time frame of interest versus that predicted from wearout.

While his effort to deemphasize the stress placed on fatigue failure mechanisms probably is warranted, one must recognize that certain forms of fatigue; e.g., thermal fatigue, may lead to failure in very short time intervals. If one limits fatigue to wear out, he has valid arguments.

FIGURE 12.8.6. The Bathtub Curve Related to Pressure Vessels

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CHAPTER 13

FACTORS INFLUENCING RELIABILITY
OF FLAW DETECTION

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CHAPTER 13

FACTORS INFLUENCING RELIABILITY OF FLAW DETECTION

13.1

INTRODUCTION

This chapter will concentrate on three areas: 1) factors influencing the reliability of the specific class of piping containing austenitic stainless steel weldments; 2) actual data pertinent to reliability flaw detection; and 3) probability models suggested for flaw detection and sizing as well as mathematical analyses of factors influencing reliability of detection. In the case of stainless steel weldments, bimetallic and trimetallic weldments will be evaluated also. Emphasis will be on factors contributing to the unreliability rather than a quantification of reliability. The actual reliability based on experimental evidence will be discussed both in the context of actual reliability values and in terms of those factors affecting reliability favorably or adversely.

With regard to models, many have been suggested to permit evaluation of the conditional probability terminology so that specific models can be compared to a common base. Both discrete and continuous distributions have been suggested for determination of reliability. If the reader wishes to review these distributions they are located in Chapter 11. The mathematical derivations of models are contained in Chapter 13A. Models specific to the stainless steels are covered in Chapter 13B. The limitations inherent in the correlation of ultrasonic signal and flow characteristics cited in Chapter 13A introduction need to be recognized. Hopefully, future work will lead to meaningful correlations. Existing data do not support generic application to predict flaw characteristics from the signals.

FACTORS INFLUENCING RELIABILITY OF FLAW DETECTION

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Weldments are inflicted with a variety of problems adversely affecting the reliability of UT. Some obvious examples due to reflectors in weld root, crown, bore and/or combinations of these factors are given in Figure 13.2.1.

x CONVERTED TO LONGITUDINAL

FIGURE 13.2.1. Geometric Reflectors in Pipe-to-Pipe Welds

If the added variable of austenitic stainless steel in base metal and weldment is included, the adverse effect on UT reliability is substantial. The following sections will examine the various factors contributing to the increased unreliability of UT in austenitics.

An approach of considerable merit being pursued in Japan is to reduce the total number of weldments subject to UT and to enhance the accessibility of these remaining joints.^(13.2.1) Figure 13.2.2 compares a conventional design to the new design. The number of welds is reduced by more than a factor of two in this one header. Increased accessibility is provided, together with elimination of longitudinal welds as can be seen in Figure 13.2.3.

13.2.1 Weldments and Joint Designs

A variety of pressure boundary joints can occur in the primary and secondary systems in a LWR. The combinations shown in Table 13.2.1 are known to exist in current reactors.

The materials cited in Table 13.2.1 may be combined in a variety of fashions. This is apparent in the bimetallic and trimetallic weldments illustrated in Figure 13.2.4.

Joint designs are not too obvious in Figure 13.2.4. They are more apparent in Figure 13.2.5, illustrating typical Vee and modified U joint designs, applicable to both ferritic and austenitic piping.^(13.2.2) A consumable-insert-shop weld in a 304SS pipe is shown in Figure 13.2.6, including the welding conditions.^(13.2.3)

Returning to a finished joint, Figure 13.2.7 gives some idea of the post-weld metal removal to prepare the weldment for UT.^(13.2.2) Such machining tends to eliminate root defects, as well as notches or crevices associated with chill rings plus improving reliability of NDE.

13.2.2 Welding Processes

As will be noted later, austenitic stainless steel weldments suffer from a number of limitations with regard to UT. Specifically, the large grain size and dendritic structures cause severe and variable beam attenuation as well as beam redirection. Some obvious corrective measures would be to refine the

FIGURE 13.2.2. Piping Design for Ultrasonic Inspection

grain size and eliminate severe dendritic orientation. Unfortunately, when one examines the spectrum of weldments, both shop and field, the type of welders and welding equipment available and limitations inherent in the construction process, the probability of replacing the currently favored processes of submerged-arc, tungsten-inert-gas, and manual metal arc appears quite low. It

CONVENTIONAL DESIGN	IMPROVED DESIGN	ADVANTAGES
<p>LONGITUDINAL WELD</p>		<p>(1) LONGITUDINAL WELD SEAMS AND CIRCUMFERENTIAL SEAM ARE DELETED.</p> <p>(2) STRAIGHT PROLONGATION (l) IS PROVIDED FOR UT.</p>
<p>(1) STRAIGHT PROLONGATION (l) IS PROVIDED FOR UT.</p>
<p>NOZZLE WELD</p>		<p>(1) PIPE TO NOZZLE WELD SEAM IS DELETED.</p> <p>(2) BRANCH IS PROLONGED (l) FOR UT.</p>

FIGURE 13.2.3. Example of Large Size Piping Components

TABLE 13.2.1. Materials Combinations Existing in LWR Pressure Boundary Weldments

Base Metal

Austenitics - wrought, cast
 Ferritics - wrought, cast

Weld

Austenitic - 309, 308L, 316L, etc., depending on base metal combinations
 Other - Inconel usually alone

Cladding

Stainless Steels - 312L, 308L, etc.
 Inconel

FIGURE 13.2.4. Typical Safe-end Weld Designs and Material Combinations

would be surprising to see any substantial changes in welding processes used to join nuclear piping. An EPRI report^(13.2.4) discusses mechanized welding processes for piping such as gas metal-arc, upset butt, flash, friction, electron beam and laser. The authors listed several advantages. They also cite an extensive listing of limitations.

13.2.3 Weldment Structure

Figures 13.2.4 and 13.2.6 illustrate two different joints leading to differing heat flow patterns which result in different dendritic grain patterns.

$T = 1/8 \text{ IN. TO } 3/8 \text{ IN.}$
 $A = 75^{\circ} \text{ TO } 90^{\circ}$
 $C = 1/14 \text{ IN. } \pm 1/32 \text{ IN.}$
 $D = 0 \text{ TO } 1/16 \text{ IN.}$

$A = 20^{\circ} \text{ TO TANGENT POINT OF RADIUS } R. \text{ ABOVE } 3/4 \text{ IN.}$
 $\text{MANY WELD PREPARATIONS USE A RANGE OF } 0^{\circ} \text{ TO } 20^{\circ}$
 $T = 3/8 \text{ IN. AND ABOVE}$
 $R = 3/16 \text{ IN. TO } 1/4 \text{ IN. RADIUS}$
 $C = 0.030 \text{ IN. TO } 0.090 \text{ IN.}$
 $D = 0 \text{ TO } 1/32 \text{ IN.}$
 $F = \text{INSIDE COUNTERBORE (COMMON) TO ASSURE TOLERANCE}$

$G = \text{TANGENT POINT OF RADIUS } R$
 $H = \text{TANGENT ON ROOT FACE } 1/8 \text{ IN. TO } 5/32 \text{ IN.}$

FIGURE 13.2.5. Flat Root-Face Type of Edge Preparation. Root-face thickness on the thin type usually range from 0.030 to 0.062 inch and on the thick type from 0.062 to 0.090 inch. Most of the flat root-face types can be used with or without consumable inserts.

PROCESS	AUTOMATIC GAS TUNGSTEN-ARC WELDING	WELDING POSITION	HORIZONTAL-ROLLED PIPE
JOINT TYPE	CIRCUMFERENTIAL BUTT	ARC STARTING	HIGH FREQUENCY
WELD TYPE	SINGLE-V GROOVE	ARC LENGTH	$3/32 \pm 1/32$ in.
POWER SUPPLY	300-AMP TRANSFORMER-RECTIFIER (a)	CURRENT (dcsp):	
AUXILIARY EQUIPMENT	SEQUENCE TIMER (b)	PASSES 1 and 2	190 amp
ELECTRODE HOLDER	300 AMP, WATER COOLED (c)	PASS 3	200 amp
ELECTRODE	1/8-in. -diam EWTh-2 (d)	PASSES 4 and 5	210 amp
FILLER METAL:		PASSES 6 and 7	220 amp
ROOT PASS	CONSUMABLE INVERTED-T-SHAPE	PASSES 8 and 9	215 amp
	INSERT RING, TYPE 308L	VOLTAGE (all passes)	11v
PASSES 2 to 9	0.035-in. -diam ER308L	FILLER-WIRE FEED RATE	45 ipm
SHIELDING GAS:		PREHEAT, POSTHEAT	NONE
AT TORCH	ARGON, AT 20 cfh	INTERPASS TEMPERATURE	350 F max
PURGE BACKING	HELIUM, AT 10 cfh	WELDING SPEED	7 ipm

(a) WITH HIGH FREQUENCY, UPSLOPE AND DOWNSLOPE CONTROL, AND REMOTE-CONTROL PANEL. (b) FOR PROGRAMMING AND PRESETTING OF GAS PREFLOW AND POSTFLOW, WIRE FEED AND WELD TIME. (c) MACHINE TYPE WITH GAS LENS, 1/2-in. -diam CUP. (d) TAPERED TO 0.010-in. DIAMETER OVER A 1/4-in. LENGTH AT TIP.

FIGURE 13.2.6. Housing for a Nuclear-Reactor Component (Top) that was Welded in the Lathe Setup Shown, to Maintain Critical Alignment, and for Which Consumable Insert Rings were Used for Root Passes, to Ensure Weld Soundness

FIGURE 13.2.7. Joint Designs for Nuclear Applications Employing Bimetallic Connections

In Figure 13.2.6, the consumable insert is melted in situ. Subsequent weld beads are applied in the pattern noted leading to limited melting of the substrate beads and preferential growth in a pattern such as given in Figure 13.2.8^(13.2.5) Simplistically, this results in a columnar of fibrous grain structure.

In Figure 13.2.4, the initial weld beads are applied on the side rather than the bottom of the carbon steel joint. This means the heat flow pattern is into the wall. Subsequent weld beads duplicate the heat flow pattern. The closure weld between the stainless steel pipe and the weld buildup is completed from the root up leading to a dendritic pattern in this region comparable to that in Figure 13.2.8(b).

In essence, the solidification process during welding initially produces a columnar grain structure in each weld bead. Preferential growth of the grains occurs along the maximum thermal gradients in the weld bead; this growth occurs along the family of $\langle 100 \rangle$ crystallographic axes. Growth along this family of axes is much faster than in other directions, leading to the disappearance of unfavorably oriented grains. In contrast to a ferritic weld, where the columnar grain structure is destroyed by the austenite-ferrite phase transformation occurring on cooling, the austenitic alloys undergo no such transformation so that the columnar grain structure not only survives, it grows, with new grains growing epitaxially on prior grains. This results in long columnar grains with their major axis along a $\langle 100 \rangle$ crystallographic direction. This is denoted as a fiber texture.

13.2.3.1 Dendritic Structure Versus Welding Technique

A given welding process may vary substantially in welding parameters such as heat input and deposition rate. Another significant variable is the direction of deposition with respect to the substrate. Tomlinson et al.^(13.2.6) and Baikie et al.^(13.2.7) investigated three different manual metal arc techniques; namely, 1) a flat or downhand weld on a horizontal substrate; 2) a vertical-up weld; and, 3) a horizontal-vertical weld. Figure 13.2.9 presents the heat flux pattern for two welding techniques, the horizontal-vertical and the downhand. The following is quoted from reference 13.2.6:

FIGURE 13.2.8(a). Schematic Diagram Showing Laying Down of Weld by Consecutive Runs

FIGURE 13.2.8(b). Schematic Diagram of Columnar or Fibrous Grain Structure in Austenitic Welds

FIGURE 13.2.9. Heat Flux in Horizontal-Vertical Weld and Downhand Weld

The local direction of growth of the columnar grains is governed by the thermal gradient within the weld bead. This, in turn, is influenced by the welding procedure and within each bead, the gradient is determined by the solid metal in contact with the bead as it cools. For example, consider weld metal deposited in horizontal runs onto a vertical plate - the horizontal vertical welding position - as illustrated in Figure 13.2.9(a). It can be seen that each weld bead is deposited into the corner formed by the preceding bead and the previous layer of beads. Heat, therefore, flows out of the bead in the two directions shown and the resultant thermal gradient and therefore the direction of crystal growth is at an angle to the horizontal. In practice the welder must deposit sufficient metal in each run to enable the following bead to be deposited on the 'ledge' formed by the preceding run and we find that the angle the grains make to the horizontal is typically about 30° . The angle of grain growth can be predicted by similar reasoning for other welding techniques. If the base plate is rotated away from the vertical, the height of the step into which new weld metal is deposited becomes progressively smaller as the plate approaches the horizontal until the downhand position shown in Figure 13.2.9(b) is reached. When the plate is horizontal, the direction of grain growth is found to be about 15° from the normal to the plate.

Figure 13.2.10 illustrates the preferred orientation fiber structure obtained with a horizontal-vertical weld. In Figure 13.2.10(b), the fiber axes resulting from the various welding techniques are presented relative to three orthogonal directions, L^+ , W^+ , and P^+ , which refer respectively to the direction perpendicular to the plane of weld layers (L^+); the direction of welding (W^+); and the direction of adding successive weld runs within each layer (P^+). The welding direction has some influence in the vertical-up technique. The direction of weld buildup is most influential with regard to the horizontal-vertical weld. The stereographic net in Figure 13.2.10(b) shows the orientation of the poles of the fiber axes.

In piping, the weld will be made up of a combination of downhand and vertical-up in a horizontal run. This means the pole orientations will vary from position to position with respect to the fiber axes.

The preceding two-dimensional description of the solidification process and the structures developed are somewhat clearer as illustrated three-dimensionally in Figure 13.2.11. An examination of this figure not only helps

FIGURE 13.2.10(a). $\langle 100 \rangle$ Pole Figure Obtained from a Horizontal Section of the Horizontal-Vertical Weld Pad

FIGURE 13.2.10(b). Stereographic Projection for the Generalized Scheme of Orientations in Experimental Weld Pads

FIGURE 13.2.11. Three-Dimensional Presentation of Dissimilar Metal Weld Illustrating Heat Flow Pattern and Epitaxial Growth of Dendrites Typical of Manual Metal Arc Weld

in understanding the downhand process, it helps in visualizing the weld buttering operation. The directions used in Figures 13.2.9 and 13.2.10(b) are readily apparent as is the orientation of dendrites resulting from a downhand weld.

A problem of concern is the UT response in a bimetallic or trimetallic weld such as those described in Figure 13.2.4.

Again, Figure 13.2.11 permits a visualization of the buttering operation. The buttering operation is similar to that described as horizontal-vertical welding displayed in Figure 13.2.9 and noted as deviating 30° from the horizontal in Figure 13.2.10(b). One may ask the question as to how much the weld preparation angle influences the heat flow pattern. A typical weld preparation of the Vee type has a 70° included angle or 35° from the vertical. This means the buttering will be at that angle. A specific example discussed by Launay et al. (13.2.18, 13.2.19) is considered pertinent. Two bimetallic welds are given in Figure 13.2.12. Results of UT examination in the buttering, weld and cast stainless steel are given in Figure 13.2.13. An examination of the velocity maxima and minima and attenuation maxima and minima confirms that the downhand weld has a fiber axis orientation quite similar to the values of 15° cited by Baikie et al. (13.2.7) and Tomlinson et al. (13.2.8). If one assumes that the buttering weld is analogous to a horizontal-vertical weld, it is possible to predict the position of the $\langle 100 \rangle$ dendrite orientation. This is

(a) WELDMENT I

(b) WELDMENT II

(SA508c13)

- A FORGED FERRITIC STEEL
- B₁ 24 Cr - 12 Ni (FIRST LAYER)
- B₂ 20 Cr - 10 Ni - 3 MO. (OTHER LAYERS)
- C 20 Cr - 10 Ni - 3 MO.
- D FORGED AUSTENITIC STEEL
- E₁ 24 Cr - 12 Ni (FIRST LAYER)
- E₂ 20 Cr - 10 Ni - 3 MO. (2ND LAYER)

- A CAST FERRITIC STEEL SA 216 GRADE WCC
- B₁ 24 Cr - 12 Ni (FIRST LAYER)
- B₂ 20 Cr - 10 Ni - 3 MO. (OTHER LAYERS)
- C 20 Cr - 10 Ni - 3 MO.
- D CAST STAINLESS STEEL (Z3 CND 17-12)
- E₁ 24 Cr - 12 Ni (FIRST LAYER)
- E₂ 20 Cr - 10 Ni - 3 MO. (2ND LAYER)

FIGURE 13.2.12. Examples of Trimetallic Welds all Manual Metal Arc (mma) Covered Electrode Welds Except Cladding (E) in (a)

believed to be at 70-72° compared to 60° using the position shown in Figure 13.2.13. This would indicate that the heat flow tends to behave as if the substrate is vertical even when it is some finite angle off the vertical. The preceding is believed to explain the cause of the fiber axis orientations. The next step has to do with UT beam deviation because of the anisotropic behavior of the dendrites.

FIGURE 13.2.13. Ultrasonic Propagation in the Different Zones of the Weld Joint in Weldment II. Zone A (No. 1), the ferritic casting is essentially isotropic in attenuation and velocity (5900 m/s).

FIGURE 3.2.13. Ultrasonic propagation in the different zones of the weld joint in weldment II. Zone A (No. 1), the ferritic casting is essentially isotropic in attenuation and velocity (5900 m/s).

13.3.1 Definitions

The common materials in nuclear piping systems are ferritic or austenitic steels. Our interest is in the acoustic response of the various systems examined with UT. This section will define relevant terminology needed to better understand the following two sections dealing with structurally sensitive response of UT. Since the concern is with acoustic response of UT, the definitions are biased toward that characteristic.

An isotropic medium is one in which properties are the same in whatever direction they are measured. Examples of properties would be attenuation or velocity of UT. A good approximation of an isotropic medium is a fine-grained randomly oriented steel, either austenitic or ferritic.

Anisotropic media are ones in which various physical properties differ in different directions. In terms of acoustic response, systems such as cubics, which are usually considered symmetric, are considered anisotropic. Isometric systems are those crystals referred to three equal and mutually perpendicular axes. Cubics represent the classic example. Cubic media have an orthogonal lattice with six symmetrically equivalent faces mutually perpendicular to one another. Orthogonality refers to a system of three mutually perpendicular axes. Orthotropic symmetry has been defined as a solid behaving isotropically about a specific axis of revolution.

Orthotropic appears to be a "coined" phrase not defined as above in either unabridged or technical dictionaries. Some hexagonal crystals display "orthotropic symmetry" about the z-axis. Of greater interest are the dendrites in an austenitic stainless steel weldment. Such dendrites are preferentially oriented in the $\langle 100 \rangle$ direction. While there is a strong $\langle 100 \rangle$ orientation, the crystallites distributed about this axis of symmetry tend to be randomly arrayed so that beams occulting the dendrite normal to the axis of symmetry may behave as if the medium is isotropic.

13.3.2 Cubic Systems

The cubic systems encompass a large number of elements and metallic alloys. The significant forms are body-centered cubic (BCC) exemplified in iron and chromium, and in the ferritic alloys, and face-centered cubic typical of nickel and the austenitic stainless steels. Figure 13.3.1 presents the two cubic systems as unit cells in an open lattice as well as hard-ball models. Less symmetric systems such as hexagonal-close-packed (HCP), orthorhombic,

FIGURE 13.3.1. Examples of (A) Body-Centered Cubic Unit Cell; (B) Hard-Ball Model BCC; (C) Face-Centered Cubic Cell; (D) Hard-Ball Model FCC

etc., are unimportant in nuclear systems. (Obvious exceptions are HCP zirconium alloys for fuel cladding and HCP titanium alloys for some fission and fusion applications.)

Figure 13.3.2 defines the significant families of planes in cubics. These are the (100) , (010) , (001) ($\bar{1}00$), $(0\bar{1}0)$ ($00\bar{1}$) where the family is denoted by $\{100\}$. The (011) is given in Figure 13.3.2(B) and (111) in (C). Again, the families are denoted by $\{110\}$ and $\{111\}$. As noted the planes are identified by sets of integers. These are obtained from the intercepts that the planes make with the coordinate axes (x , y , z). The Miller indices are proportional not to the intercepts but to their reciprocals. By definition the Miller indices are the smallest integers having the same ratios as the reciprocals. A (111) plane represents intercepts at the unit cell positions. Intercepts of 1 on x , 2 on y and ∞ on z would become $(1, 1/2, 0)$. The integers become (210) .

Directions are relatively straightforward in cubic systems. For example, Figure 13.3.3 illustrates the most common directions $[100]$, $[001]$, $[010]$, $[110]$, $[\bar{1}10]$, and $[111]$. The family of directions are given by $\langle 100 \rangle$.

The terminology for directions, direction numbers and direction cosines has been given previously in Figure 9A.8. The figure is repeated here as

FIGURE 13.3.2. (A) Cube Planes of a Cubic Crystal: $a(100)$; $b(010)$; $c(001)$; (B) the (011) Plane; (C) the (111) Plane

FIGURE 13.3.3. Commonly Used Directions in Cubic Crystals

Figure 13.3.4 together with the concept of wave-front normals and energy flux in a solid medium. The directional changes in waves will be covered in Chapter 13B.

One final area is that of stereographic projections. An example was given in Figure 13.2.10. Figure 13.3.5 does an excellent job of defining poles and planes in a stereographic projection for the more common poles and planes; namely, (100), (110), (111).

$$\begin{aligned} \ell &= \cos \alpha \\ m &= \cos \beta \\ n &= \cos \gamma \end{aligned}$$

FIGURE 13.3.4. (A) Terminology of Direction Numbers and Direction Cosines Referred to Reference Axes X, Y, Z . (B) Visualization of the Wave-Front Normal and the Direction of Energy Flux, i.e., "Propagation Direction." (C) Modification of (A) Showing Velocity, Direction Change for Waves A_1, A_2, A_3 (Longitudinal and Shear Waves)

FIGURE 13.3.5. Stereographic Projections of Several Important Planes of a Cubic Crystal. (A) Cubic System, the (100) Plane, Line of Sight Along the [100] Direction. (B) Cubic System, the (110) Plane, Line of Sight Along the [100] Direction. (C) Cubic System, the (111) Plane, Line of Sight Along the [100] Direction

An attempt will be made in this section to establish the relative importance of various factors as they influence the reliability of ultrasonics in the examination of austenitic alloys. Emphasis is on weldments; however, wrought and cast products as well as single crystals are included. With regard to NDE procedures only conventional UT will be reviewed in this section.

13.4.1 Test Pieces

Table 13.4.1 contains a listing of materials examined and their form, whether wrought, cast welded, etc. A review of the references cited in Table 13.4.1 confirms that the major emphasis is on weldments. Included are austenitic, bimetallic and trimetallic weldments permitting an assessment of the relative effects of such weldments. Limited data are contained in Table 13.4.1 with regard to the effect of welding process on the macrostructure as it may influence UT reliability. For example, a metal-inert gas process yields fan-shaped grains rather than the columnar dendrites typical of manual metal arc. While several specimens are included in the table, it is apparent that there is a dearth of information in the open literature with regard to the effect of various welding processes on UT reliability.

13.4.2 Interactive Factors Influencing the Ultrasonic Signal

Coarse-grained highly oriented austenitic alloys pose substantial problems with regard to the use of UT. Such factors as signal attenuation, beam velocity, angular deviation of the beam, beam convergence or divergence, and signal-to-noise ratio influence detection reliability, either as separate factors or interacting one with another. Each of these factors will be examined in the following subsections.

13.4.2.1 Attenuation and Scattering

The combination of large grain size and fiber texture typical of welds and castings lead to much higher attenuation than observed in wrought alloys of similar composition. Scattering will contribute to the observed attenuations with reflection, refraction and mode conversion contributing to scattering. Both Rayleigh and stochastic scattering can contribute to the overall effect

TABLE 13.4.1. Examples of Austenitic and High Nickel Alloy Materials, Primarily Weldments Examined with Ultrasonics

Materials	Thickness, mm	Welding Process	Weld	Joint Design	Structure	Reference
304/308	50	SMA Downhand	Butt	30°V	Vertical Dendrites	13.4.1
316	39	SMA-Large Grain SMA-Fine Grain SMA-Medium Grain-High-C SMA-Medium Grain-Low-C	Butt	?	?	13.4.2
316	39	?	Butt	Double V		13.2.5
316	?	MMA	Downhand	None	<100> Fiber	13.2.7
316			Vertical Up		<100>	
316			Vertical-Horizontal		<100>	
316		MMA	Fillet			
2-1/4 Cr-1 Mo Inconel-182 - 316	50	MMA	Downhand	Butt-J	Columnar	13.2.6
Nimonic 80A	--		Single X-tal		--	
316-316-2-1/4 Cr-1 Mo	50	MIG	Downhand	Butt-V	Fans	
304	40	MMA SMA		Butt-V Butt-Double V	Dendritic	13.4.6
CF8 Cast	50				Coarse Grain	13.4.7
304/308	70	SMA	Butt-V			13.4.8
304	40	EB				13.4.9
Cast Stainless Steel					Columnar	13.4.10
316					Columnar	
304		Wrought-Coarse to Fine Grain				
316		Wrought				
Stainless Steel			Single X-tal			13.4.11

TABLE 13.4.1. (contd)

Materials	Thickness, mm	Welding Process	Weld	Joint Design	Structure	Reference
304	?	SMA		Overlay	Dendritic	
Cast Stainless Steel						
Cast Ferritic-24-Cr-12Ni-20 Cr-10Ni-316 SS-Cast 316 SS (CF8)	60	MMA		Butt-J	Dendritic	13.2.8
Forged SA508-24Cr-12Ni 20Cr-10Ni-3Mo-Forged Aust. 244Cr-12Ni a 20Cr-10Ni-3Mo Cladding	120	MMA		Butt-V	Dendritic	13.2.9
SA 216 Cast Buttering and Weld As Above - Cast 17-12 Cladding as Above	120	MMA		Butt-V	Dendritic	
316	50	MMA	Downhand	Butt-V	Dendritic	13.2.12
316	50	MMA	Downhand	Butt-V		
316	25	SMA		Butt-V	Discontinuous	
316	50	MMA	Horizontal Vertical	Butt-V		13.4.3
		TIG		Butt		
		EB		Butt		
A 351 Cast (CF8M)	86				Dendritic	13.4.4
Cast	70				Dendritic	
Trimetallic Ferritic Overlay	62				Dendritic	
Aust.-Weld Aust-Overlay Ferritic						
316-Weld-CF8m	36	?		Butt-V		13.4.5
304-308	50	SMA		Butt-V-77°	Dendritic	
Single Crystals 19 Cr-10Ni; 19Cr-14Ni; 19Cr-19Ni						

where Rayleigh scattering varies as the third power of grain size and inversely as the fourth power of wave length. Stochastic scattering varies directly with grain size and inversely as the square of wave length. Scattering increases background noise reducing signal-to-noise ratio.

In the case of gross attenuation as functions of wave length, Figures 13.4.1(a)^(13.4.6) and 13.4.1(b) permit comparisons of weld and base metal as well as cast materials. While the difference between shear and longitudinal waves is not significant, that of higher frequencies (lower wave lengths) is. Figure 13.4.2 permits a comparison of grain size alone on attenuation.^(13.4.10) Materials 1 and 2 appear to be European versions of 321SS in wrought form.

The problem of strongly oriented structure is apparent from Figures 13.4.3 and 13.4.4.^(13.2.6) In Figure 13.4.3, the dendritic structure typical of welds leads to a periodic pattern of attenuation maximizing at 0° and 90° with respect to the fiber axis and minimizing at 45° . Typically, the weld dendrite fiber axes are approximately normal to the surface for a downhand weld deposited in the usually butt weld preparation. Figure 13.4.4 presents data for a single crystal of nimonic 80A. The similarity of signals in the two figures is considered conclusive evidence that grain boundary scattering is not a major contributor to variation in signal through weld metal. Scattering will be more important in signal-to-noise ratio.

13.4.2.2 Wave Velocity

Changes in attenuation tend to be accompanied by similar changes in wave velocity. As the velocity increases the attenuation decreases. Figure 13.4.5 shows the longitudinal wave velocity maximizing at 45° to the fiber axis, the location of lowest attenuation.^(13.2.6) Of greater interest is the relationship over a spectrum of grain orientations such as shown in Figure 13.4.6.^(13.4.4) Attenuation tracks velocity fairly well across dissimilar metal interfaces, different direction of dendritic orientation and differing welding techniques.

FIGURE 13.4.1(a). Ultrasonic Attenuation of Austenitic Steels as a Function of Frequency for Longitudinal Waves

FIGURE 13.4.1(b). Measurement of Ultrasonic Attenuation: Above Weld Metal; Below: Base Metal; $\square$ - Shear Waves, $\circ$ - Compression Waves. Attenuation is higher for shear waves at higher frequencies (lower wavelengths).

FIGURE 13.4.2. Attenuation Measurements of Materials 1 and 2 for Different Grain Sizes

The sensitivity of shear-wave velocity to orientation is shown in Figure 13.4.7 where single crystals and dendritic structures of nickel single crystals are intercompared.^(13.4.5) This effect is covered in greater detail in Chapter 13B.

Table 13.4.2^(13.4.11) presents velocity and attenuation data obtained with UT at frequencies of 2 and 10 MHz using both shear and longitudinal waves. A stainless steel weld, casting and single crystal, were examined at various orientations with respect to beam and fiber, or crystallographic axes. The attenuation increased substantially at higher frequencies, confirming the usual position of testing at lower (1- to 2-MHz) frequencies.

The shear-wave velocity was found to depend strongly on both beam propagation and polarization direction. The significance of this is most apparent in the degree of grain boundary scattering which is much more pronounced in shear than in longitudinal waves. In a $\langle 100 \rangle$ direction, shear-wave velocity is independent of polarization direction. In a textured material, where shear propagation is perpendicular to the fiber axis, shear velocity will be equal in all grains and attenuation will be quite low.

FIGURE 13.4.3. Variation of Transmitted Signal with Direction Relative to Columnar Grain Axes

FIGURE 13.4.4. Through-Transmitted Signal in a Single Crystal of Nimonic 80A Versus Angle Between Wave Normal and $\langle 100 \rangle$ Axis

FIGURE 13.4.5. Variation of Compression Wave Velocity with Direction Relative to the Fiber Axis in 316 Weld Metal

FIGURE 13.4.6. Examples of Variations in Wave Velocity (b) and Attenuation (c) in a Bimetallic Weld Schematically Illustrated in (a)

FIGURE 13.4.7. Velocity-Surface Intersection with (110) Plane for (1) Single Crystal Stainless Steel; (2) Dendritic Structure of Stainless Steel; (3) Nickel (analytically)

The preceding comments are based on the observed response in velocity and slowness surfaces. (13.4.11) The details of slowness and velocity surface behavior are covered in Chapter 13B.

Gray et al. (13.4.3) cite the behavior of shear waves in the base-metal-weld interface region. The signal from the interface is greater than that for a 1.5-mm side-drilled hole (SDH) in the weld metal; with longitudinal waves the opposite is true. A qualitative explanation considers two effects: 1) there is a 25% difference between shear-wave (S_v) velocity in base metal and in the 45° direction in weld metal, but only 5% difference for longitudinal waves. Therefore, the acoustic impedance mismatch at the fusion boundary will be larger for S_v than L-waves; (2) Longitudinal waves are weakly reflected by right angle corners so strong shear-wave components will be produced by mode conversion.

TABLE 13.4.2. Velocities and Attenuations Measured at 2 and 10 MHz. Shear and longitudinal wave for single and polycrystalline (dendritic) stainless steel specimens (nominally 304, 316) (Reference 13.4.11)

Frequency 2 MHz (30 x 30 x 30 mm Cubic Specimens)

Material	UT Mode	Direction	Polarization	Velocity, m/s	Attenuation, dB/cm
Weld	Long	0°	--	5000-5030	1.2-1.3
Weld		45°	--	6000-6040	0.7-0.8
Weld		90°	--	5140-5280	2.3-2.8
Casting	Long	0°	--	5220	1.8
Casting		45°	--	6140	1.0-1.3
Casting		90°	--	(a)	(a)

Frequency 10 MHz Plate (Weld, Single X-tal) or Sphere (Single X-tal)

Weld	Long	0°	--	5308	5.9
Weld	Trans	0°	90°	3960	7.4
Weld	Long	45°	--	6228	6.0
Weld	Trans	90°	0°	4060	9.7
Weld	Long	45°	--	6287	5.8

Single Crystal

Plate	Long	[110]	[110]	6085	0.67
Plate	Trans	[110]	[001]	4014	1.17
Plate	Trans	[110]	[110]	2086	1.87
Sphere	Long	[111]	[111]	6300	--
Sphere	Long	[110]	[110]	6080	--

(a) No back-wall echoes obtained.

The preceding has been observed in electron beam welds and to a degree with tungsten-inert gas welds.

13.4.2.3 Beam Deviation

Yoneyama et al. (13.4.1) investigated the behavior of a 45° shear wave in an austenitic weldment. Figure 13.4.8 permits a comparison of beam behavior in a) base metal, b) through base metal into a weld, and c) completely in weld metal. In base metal, wave propagation was as anticipated; namely, close to

FIGURE 13.4.8. Path of a Wave Train Through (A) Base Metal; (B) Weld Zone for Case Where a False Echo was Received; (C) Weld Deposited Metal Alone

the initial angle of 45° . The same behavior occurred in the base metal of b) to the base-metal-weld interface. At this point the beam direction shifted and followed the fiber axes of the dendrites. The same pattern of paralleling the fiber axis orientation was seen in 100% weld metal, shown in Figure 13.4.8(c). The authors^(13.4.1) attribute the beam bending to a phenomenon akin to that in wave guides; the shear-wave beam turned gradually away from the direction of incidence as if it were being directed through channels following the dendritic microstructure.

The topic of beam deviation, and the following one on beam convergence and divergence, are inextricably interwoven. The one leads to the other. As a result the reader should consider the subject in the context of both areas.

Hudgell et al.^(13.4.12) discusses UT-beam deviation (or skewing as they term it). In anisotropic media (such as an austenitic weld) three different waves are possible. Two are quasi-shear and the third is quasi-longitudinal where the "quasi" refers to the fact that the wave front is not at right angles to the direction of propagation. The bases for this behavior will not be discussed further here. They are the topic of an extensive review in Chapter 13B.

Angles of deviation can be calculated for longitudinal and shear waves as functions of beam to columnar grain angle. This has been done for various austenitic alloys. Figure 13.4.9 presents some such data. Figure 13.4.10 is a compilation from several sources.^(13.2.5, 13.4.5, 13.B.2) The similarity of behavior for various materials is quite apparent.

Figures 13.4.11(a), (b), and (c)^(13.4.12) contain data on two butt welds fabricated with essentially the same manual metal arc downhand procedure. UT behavior was similar to that of Yoneyama.^(13.4.1) The beam angle in the weld metal often differed from that in the base material and it was not possible to control the angle in the weld by changing the probe wedge angle. In Figure 13.4.11(a), it can be seen that the longitudinal waves propagate preferentially at 45° to the fiber axis. Figure 13.4.11(b) presents the direction of the most intense portion of the beam for detection of holes at the interface. As noted, a normal L wave used with a weld having nominally vertical dendrites may be subject to beam bending. Figure 13.4.11(c) illustrates preferred paths for the detection of both centerline and fusion boundary holes. In Specimen 2,

FIGURE 13.4.9. Skewing Angle Δ as a Function of the Angle ϕ Between the Normal to the Wavefront and the Columnar Grain Axis

the preferred paths form a orthogonal set because the dendrites are vertical. In Specimen 1, the paths are distorted because the dendrites are neither straight nor vertical. This latter indicates that welds produced by essentially the same procedure may yield markedly different UT results.

UT results differed depending upon whether the beam passed entirely through weld metal or passed through a fusion boundary. The mean deviation of the average beam angles were less when completely within weld metal, indicating that beam scattering occurred due either to an uneven fusion boundary or to curved grains along the boundary.

The preferential paths of L waves in the welds is attributed to the beam skewing cited in Figures 13.4.9 and 13.4.10. If the beam to fiber axis angles are 17° to 44° , the angle of deviation between the direction of propagation and

FIGURE 13.4.10. Variation in the Angle of Deviation Δ in the (100) Planes for (1) Single-Crystal Stainless Steel; (2) Iron; (3) 316 SS Weld Metal; (4) Nickel (analytically)

the wavefront will be positive. This will increase the beam angle by some amount which decreases with the angle. At 45° , the deviation will be zero. For angles between 46° and 75° , the deviation will be negative reducing the beam angle. At 45° , the L-wave paths will be stable--these have been called preferred paths. This results in beam convergence discussed further in the next section.

Beam skewing is zero at 0° and 90° (Figure 13.4.9); however, beam angles of only a few degrees on either side of these values lead to a large skewing effect. If the angles are 0° to 15° , the wavefront deviation will be positive

FIGURE 13.4.11. Examples of Preferential Beam Skewing in 316 SS. Weldments for 2.25 MHz longitudinal wave: (a) preferred paths; (b) preferred paths when penetrating weld from opposite direction; (c) direction or most intense portion of beam using normal and angled probes

and increasing with beam angle; therefore, the beam will diverge and signal-to-noise ratios will be poor. At 75° to 90° , where the deviation is negative and decreasing, there will be the same effects.

The analysis of beam deviation by Silk et al.^(13.2.5) follows that of Hudgell^(13.4.12) quite closely. The authors^(13.2.5) also discuss shear-wave behavior. There will be differences compared to L waves because of the magnitude of the deviation due to anisotropy with shear waves. For example, a shear beam entering at $\sim 0^\circ$ will be focussed, but at a location well within the weld. By the time the beam reaches the opposite side, it will be diverging rapidly. This leads to the attenuation seeming to be high at both 0° and 45° . Additionally, the severe deviations at intermediate angles could lead to those being directions of highest apparent attenuation. The possible coexistence of true and quasi-shear waves arriving at similar times is a further complication.

Tomlinson et al.^(13.2.6) discusses skewing in similar terms and arrives at comparable conclusions. He makes the point covered in Chapter 13B that for a given direction of energy travel (V_e), the ray direction may not be perpendicular to the wave front in an anisotropic medium. This would lead to skewing of the beam off the axis of the transducer.

Kupperman and Reiman^(13.4.5) discuss beam deviation as well as presenting experimental data. Their comments are presented in the next section where they appear more relevant.

13.4.2.4 Beam Convergence and Divergence

Beam deviation is accompanied by convergence or divergence depending upon whether the angle is positive or negative. The preceding statement is factually true and, if we accept it as such without confirmation, the following review commentary becomes unnecessary. However, we shall retrace our steps to re-examine the orientation of dendrites formed during welding and the influence of such dendrites on wave velocity and attenuation.

Tomlinson^(13.2.6) has cited prior theory as well as experimental evidence to explain skewing. Basically, if the energy flux or ray deviates from the direction of the wave-front normal, skewing can occur. Figure 13.3.4 illustrates this condition.

Figure 13.3.4(b) represents such a deviation which is seen for longitudinal and shear waves (A_1, A_2, A_3) in Figure 13.3.4(c). Whenever energy flux and wave-front normal coincide, the deviation (Δ) passes through zero.

Figure 13.4.12 can be used to understand beam convergence and divergence. In this figure the deviations are zero at zero for both L and S waves. As the angle of deviation from the normal increases, the L-wave curve increases, goes through a maximum, and becomes zero at 45° to 50° ; then continues to decrease, passes through a minimum, and returns to zero at 90° from the normal. The

VELOCITY

MAXIMA - L WAVE $45-50^\circ$; S WAVE $0^\circ, 90^\circ$ POLARIZATION //
MINIMA - S WAVES 45° (POLARIZED // x (110)); 90° (POLARIZATION //)

ATTENUATION

MINIMUM - S WAVE $\Phi = 90^\circ$ (POLARIZED //); 0° ATTENUATION
HIGHER THAN 90° , POSSIBLY DUE TO MODE CONVERSION, GRAIN
BOUNDARY SCATTERING, AND ACOUSTIC MISMATCH WHICH IS
GREATER ACROSS DENDRITES

FIGURE 13.4.12. Examples of Effects of Beam Skewing in a Weldment With Respect to Velocity, Attenuation and Beam Spread

shear wave operates oppositely. It becomes negative, passes through a minimum, and crosses the axis near 45° ; becomes positive, reaches a maximum, and returns to zero at 90° . The preceding is a vertical shear wave. The horizontal shear wave is zero $0 \rightarrow 90^\circ$ if a (100) plane is negative from zero, returning to zero at 90° if the projected plane is other than (100).

The following description of beam behavior is a melange of several references. (13.2.5, 13.2.6, 13.4.5, 13.4.12) With an L-wave at 0° paralleling the fiber axis, there will be dispersion. The non-central rays will be skewed outward resulting in a widened lower intensity beam. (13.2.6) Near 0° and 90° , the positive slopes are quite steep. Silk (13.2.5) notes that these positive slopes reinforce the natural change in beam angle so a natural beam divergence of 5° will become 10° ; this is in qualitative agreement with the prior statement. Juva and Lenkerri (13.4.11) explain the steep slope and the rapid beam divergence of the L-wave in terms of a slowness surface such as shown in Figure 13.4.13 where the value at 0° $\langle 001 \rangle$ is a sharp peak. This means a beam having a width of a few mm will diverge strongly in the $\langle 100 \rangle$ direction.

The preceding statements generally apply at 90° .

The situation is different at 45° which is a direction of maximum velocity. The beam will converge; this can be confirmed analytically; however, calculations, except for the simplest cases, are very complex. Another way of stating the preceding is that the deviation due to anisotropy near 45° will tend to counteract the natural beam deviation. A naturally diverging beam would have its level of divergence substantially reduced so the UT beam would be much more penetrating at 45° , compared to 0° or 90° . At intermediate angles, the defocussing effects observed near 0° and 90° will not be so strong; however, the signal will be reduced because of deviation of the entire beam. Juva and Lenkerri (13.4.11) return to the slowness surface of Figure 13.4.14 to explain the relatively minor changes in convergence near 45° . The flatness of the slowness surface for angles near $\langle 110 \rangle$ means a lesser sensitivity to slight angular deviation. Even so a 2° change can reduce beam amplitude drastically. The authors attribute this to the fact that odd order wave numbers deviate to the point that the receiver only partially detects them.

FIGURE 13.4.13. Cross Sections of the Slowness Surfaces Corresponding to $\beta=0^\circ$, 15° , 30° and 45° . The small arrows in (a) and (d) represent the directions of energy flux for 316SS weldment.

In shear waves the situation is more complicated. One could argue that the attenuation should be lowest at 0° and 90° and highest near 45° on the basis that there should be sharp focussing because of the negative slopes. This argument is too simplistic. The magnitude of deviation of the shear wave ($\sim 50^\circ$) will contribute substantially to defocussing. Near 0° the beam may be focussed; however, the focus will be well below the surface and it will tend

FIGURE 13.4.14. Schematic Representation of Beam Divergence and Convergence for Longitudinal and Shear Waves with Propagation Direction Indicated by Arrows

to diverge as it penetrates more deeply. A further complication is that the true shear wave may exist concurrently.

The polarization of the shear wave is not important at 0° . However, it becomes quite important at 90° . If the polarization is parallel to the dendrites, the attenuation is markedly less than when polarized perpendicular to the dendrites.

Figure 13.4.14 schematically presents the data in Figure 13.4.12 and in the preceding paragraphs.

While convergence and divergence are important contributors to signal amplitude and attenuation, the contribution must be kept in perspective. For example, diffusive scattering in coarse-grained ($D > \lambda$) materials such as welds can be quite severe. There can be strong reflections at grain boundaries leading to strong attenuation.

One suspects that beam convergence and mode conversion might account for the apparent increase in beam intensity cited but not explained by Yoneyama et al.^(13.4.1) If the beam was redirected along the $\langle 100 \rangle$, the velocity would increase compared to velocity in an isotropic medium, leading to beam focussing and the increased intensity (Figure 13.4.15).

FIGURE 13.4.15. The Variation with Specimen Thickness of the Intensity of the Sound Wave Reaching the Back Surface of the Sample

Hudgell et al. (13.4.12) examined beam profiles in stainless steel weldments. They used a 45° L-wave to examine two butt welds. Figure 13.4.16 permits a comparison totally within weld metal. Specimen 1 produced an apparently bent beam while Specimen 2 did not. One could explain the profile on the basis that L-waves follow the curved columnar grains; however, probe movement means several grains are scanned. Figure 13.4.17 is better evidence of beam bending. In the weld, it can be seen that the UT beam bends away from the vertical and the width of the most intense portion (black) of the beam is approximately

FIGURE 13.4.16. Examples of Beam Profiles for 45° Longitudinal Waves Plotting Signals from Side-Drilled Holes Along Weld Centerline. Note bent beam in (a).

FIGURE 13.4.17. A Comparison of a Normal Probe Beam Profile in Plate and Weld Metal. Structure in weld consists of grains leading to continuous ultrasonic beam paths from one half of the weld to the other with little or no curvature.

three times that in plate material. This behavior is similar to that reported by Yoneyama et al. (13.4.1) with regard to preferred beam paths.

13.4.3 Optimal Equipment Factors for Flaw Detection

There is general agreement concerning certain factors to be controlled if a reasonable signal-to-noise ratio is to be achieved concomitant with an acceptable reliability of flaw detection. These factors are as follows:

- Select the form of UT waves that interact minimally if at all with the grain structure. (13.2.5, 13.2.6, 13.4.7) The preferred choice is longitudinal waves with optimally polarized shear waves a possibility. The L-waves also will have less skewing and scattering.

- Optimize frequency (wavelength) to yield acceptable signal-to-noise ratio together with good flaw resolution. (13.4.7, 13.4.8) Higher frequencies increase noise and decrease signal-to-noise ratio; lower frequencies (<1 MHz) decrease resolution. Optimum ranges 1 to 2.25 MHz.
- Use short broad band pulses to reduce scattering, improve signal-to-noise ratio, reduce false signals. (13.2.6, 13.4.7, 13.4.8) Low pulse lengths reduce scattering thereby improving signal-to-noise ratio; e.g., a tenfold reduction in pulse length improves signal-to-noise ratio about 10 dB. Increased bandwidth reduces false signals because of shorter signal times; this approach also reduces signal amplitude.
- Use controlled shaping of the transducer acoustic field to reduce grain scattering (beam focussing). (13.2.6, 13.4.7, 13.4.8)
- Obtain acoustic and electrical matching of transducers and flaw detector (dual probe). (13.4.7)
- Use signal processing to increase signal-to-noise ratio. (13.4.7)
Use variable frequency and bandwidth transmitter pulses. Use signal averaging as an inverse filter.
- Use adequate numbers and types of reference blocks to provide meaningful calibration.

The sensitivity of signal amplitude to frequency can be seen in Figure 13.4.18. The amplitude from a 3-mm hole decreases markedly with increasing frequency, accompanied by a limited increase in back-scattering. This limited increase is believed due to multiple scattering at high and low frequencies.

With regard to bandwidth, an austenitic coarse-grained material acts as a low pass filter. Depending upon the sound path, both effective testing frequency and bandwidth are reduced. Figure 13.4.19 graphically illustrates the influence of bandwidth on signal-to-noise ratio. The narrow bandwidth gives a very poor signal-to-noise ratio. The best compromise is the intermediate bandwidth yielding the best signal-to-noise ratio. Too wide a bandwidth leads to the high frequency portion of the spectrum contributing to grain boundary scattering.

FIGURE 13.4.18. Influence of Ultrasonic Scattering in an Austenitic Cast Material on the Flaw Detectability

Longitudinal waves are preferred for austenitic weldments because of less effects on signal-to-noise ratio. Further improvements can be had with beam focussing.

13.4.4 Anticipated Structures Resulting from Various Weld Processes

As a precursor to evaluating the reliability of detection of flaws by various UT techniques, it would be desirable to examine the macrostructures of weldments produced by various welding processes. Figure 13.4.20 schematically presents such structures for four processes, two of which are commonly used in producing austenitic weldments in nuclear components (MMA, SMA) and two of which are either less common (TIG) or very rare (EB).

As noted, MMA produces fairly continuous dendrites by epitaxial growth and the structure will be very coarse-grained. The SMA process results in coarse grains with little continuity across the interfaces of the weld beads. But TIG and EB are narrow-gap high heat input processes where grain sizes should be small due to high chill factor (particularly EB). Fiber axis orientation should be perpendicular to base-metal-weld interface.

FIGURE 13.4.19. Influence of the Bandwidth of Ultrasonic Probes
on the Signal-to-Noise Ratio

(a) MANUAL METAL ARC
EITHER DOWNHAND OR
HORIZONTAL-VERTICAL
 $P \approx 1$

(b) SUBMERGED METAL ARC
 $P \approx 1$

(c) TUNGSTEN INERT GAS
 $P < 1$

(d) ELECTRON BEAM
 $P \rightarrow 0$

FIGURE 13.4.20. Schematic Diagram of Fiber Axes in Weld Dendrites as Function of Welding Process. Probability of field use of given weld process indicated.

13.5.1 Standard UT13.5.1.1 Bimetallic and Trimetallic Weldments

- Examples of successes and failures in detecting real and artificial flaws in bimetallic and trimetallic weldments are given in this section. Figure 13.2.12 represents such weldments.
- Edelman^(13.4.8) discusses the generation of a distance-amplitude curve (DAC) in an Inconel weld in ferritic base material. SDHs were used for calibration and a 45° 4-MHz L-wave probe was used. The DAC through weld metal was markedly different from that through base metal—not too surprisingly.
- In another example,^(13.4.8) a 45° L-wave probe through a complex ferritic-buttering-weld 18/8 SS yielded a spurious indication due to deflection of a portion of the wave at the weld-buttering interface.
- Pelseneer and Louis^(13.4.4) reported on a trimetallic weldment previously illustrated in Figure 13.4.6. No welding details were given in the paper. Table 13.5.1 from reference 13.4.4 presents the data for longitudinal and shear waves, and is summarized in the following:

UT Process		Detection of	
		Bore Holes	Crack
Shear Wave	45°, 60°	None	No
Shear Wave	70°	First Hole	No
Longitudinal Twin-Crystal	45°, 60°	All	No
	70°	None	No
L-Single Crystal V-Probe	45°, 60°, 70°	All	Only by 45°
Twin Crystal V-Probe	45°, 60°, 70°	All	Only by 60°, 70°

TABLE 13.5.1. Detectability of Small Holes and a Crack at the Transition Zone Between an Austenitic and a Ferritic Weld Overlay. No suppression used.

FS-AS: SCAN IS MADE FROM THE SIDE OF THE WELD WITH OR WITHOUT FERRITIC OVERLAY (LEFT OR RIGHT SIDE OF FIG. 13.4.6)
NO RESULT INDICATED MEANS REFLECTOR NOT DETECTABLE.

		TRANSVERSE WAVES	LONGITUDINAL WAVES - SE		W4SLF - 1		W4SLF - SE - 2		TRANSVERSE WAVES	LONGITUDINAL WAVES - SE		V6OLF - 1		V6OLF - SE - 2		TRANSVERSE WAVES	LONGITUDINAL WAVES - SE		V7OLF - 3		V7OLF - SE - 2		
REFRACTED ANGLE IN FERRITIC STEEL (DEGREES)		45	45		45.8		44		60	53.1		55.7		56.9		62.8		70		70.5		64.8	
TESTING SURFACE		F.S. OR A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.	F.S. OR A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.	F.S.	A.S.
1st HOLE: 15 mm DEEP Ø 2 mm	AMPLI. LEVEL (dB)	—	44	44	34	34	44	50	—	48	50	42	42	42	58	40	62	46	48	60	62	48	58
	PULSE ENERGY (1)	—	—	—	1	1	—	—	—	—	—	1	1	—	—	2	2	—	—	1	1	—	—
	ECHO HEIGHT (% SCREEN HEIGHT)	—	80	95	90	80	90	90	—	90	80	90	80	90	85	90	90	100	100	90	100	80	85
	GRASS LEVEL (% SCREEN HEIGHT)	—	20	10	5	5	40	20	—	15	15	5	5	5	15	0	60	5	20	20	25	5	15
	MEASURED PATH LENGTH (mm)	—	20	17.5	20	20.5	22.5	23.5	—	25	26	32	28	32	31	—	31	26	26	34	31	35	26.5
2nd HOLE: 30 mm DEEP Ø 2 mm	AMPLI. LEVEL (dB)	—	48	50	32	36	40	46	—	52	52	46	46	46	56	—	—	—	—	68	62	52	56
	PULSE ENERGY (1)	—	—	—	1	1	—	—	—	—	—	1	1	—	—	—	—	—	—	1	1	—	—
	ECHO HEIGHT (% SCREEN HEIGHT)	—	90	90	80	90	90	80	—	80	90	90	90	85	85	—	—	—	—	100	95	90	90
	GRASS LEVEL (% SCREEN HEIGHT)	—	20	20	5	10	25	15	—	20	15	5	10	10	15	—	—	—	—	40	30	10	5
	MEASURED PATH LENGTH (mm)	—	38	42	41	42	40.5	37	—	53	45	58	48	60	52	—	—	—	—	85	63	69	54
3rd HOLE: 45 mm DEEP Ø 2 mm	AMPLI. LEVEL (dB)	—	52	54	34	42	40	50	—	56	56	46	50	48	56	—	—	—	—	74	80	56	64
	PULSE ENERGY (1)	—	—	—	1	1	—	—	—	—	—	1	1	—	—	—	—	—	—	1	1	—	—
	ECHO HEIGHT (% SCREEN HEIGHT)	—	90	90	80	90	85	90	—	90	90	80	95	80	80	—	—	—	—	90	90	85	90
	GRASS LEVEL (% SCREEN HEIGHT)	—	30	20	5	20	25	15	—	25	20	20	20	15	15	—	—	—	—	30	40	10	30
	MEASURED PATH LENGTH (mm)	—	53.5	59	63	60	58.5	—	—	73	63	88	71	79	69	—	—	—	—	140	107	101	83
CRACK: 60 mm DEEP 4.5 mm HIGH	AMPLI. LEVEL (dB)	—	—	—	54	60	—	—	—	—	—	—	—	68	64	—	—	—	—	—	—	72	62
	PULSE ENERGY (1)	—	—	—	1	1	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—	—
	ECHO HEIGHT (% SCREEN HEIGHT)	—	—	—	80	90	—	—	—	—	—	—	—	90	90	—	—	—	—	—	—	90	90
	GRASS LEVEL (% SCREEN HEIGHT)	—	—	—	20	60	—	—	—	—	—	—	—	60	60	—	—	—	—	—	—	10	20
	MEASURED PATH LENGTH (mm)	—	—	—	79.5	85	—	—	—	—	—	—	—	115	89	—	—	—	—	—	—	138	112.5

(1) PULSE ENERGY: ON USM2 EQUIPMENT, TWO POSSIBILITIES FOR SINGLE CRYSTAL PROBES, BUT ONLY ONE FOR TWIN CRYSTAL PROBES.

- Launay et al. (13.2.8, 13.2.9) prepared and examined at least three bimetallic weldments given in Table 13.5.2. As noted, these cover a spectrum of forged and cast components as well as buttering, weld and overlay cladding. Those three weldments will be discussed in some depth to better establish the limitations inherent in UT of such weldments.

The first weldment (I in Table 13.5.2) initially was examined with shear wave 45° . This procedure was capable of detecting the holes in the ferritic steel only. Nothing could be detected in the buttering weld or cast stainless steel. In addition, spurious signals occurred. In order to detect in the cast ferritic steel, a 25- to 43-dB gain was required.

The authors (13.2.8) found it necessary to develop equipment specifically to optimize detection of 2- and 3-mm ϕ holes. The following were the critical factors:

1. the water/steel interface
2. attenuation specific to each material for a given angle of incidence
3. the crossing of the interface between steels
4. variation in the UT path according to the angle of incidence.

The following parameters were varied:

1. the angle of incidence into steel
2. the frequency
3. the UT pulse length transmitted
4. the UT beam configuration generated by the transducer.

Item 4, is relevant to alternate rather than standard UT systems. With conventional transducers, the following were the optimum angles for 30-mm deep holes:

- a. from ferritic side - 42° to 45°
- b. from cast SS side - 30° to 37°
- c. optimum frequency - 1 MHz.

Length of UT pulse transmitted was a function of the damping of the transducer and of the electric pulse generated by the UT equipment. The optimum was a well-damped transducer with an optimized electrical pulse.

TABLE 13.5.2. Examples of Bimetallic Weldments(13.2.8, 13.2.9)

COMPONENT	WELDMENT		
	I	II	III
A	CAST LOW-CARBON FERRITIC STEEL	FORGED SA508 Cl 3	CAST SA 216 WCC
B ₁	24 Cr-12 Ni	24 Cr-12 Ni	24 Cr-12 Ni
B ₂	20 Cr-10 Ni-3 Mo	20 Cr-10 Ni-3 Mo	20 Cr-10 Ni-3 Mo
C	20 Cr-10 Ni-3 Mo	20 Cr-10 Ni-3 Mo	20 Cr-10 Ni-3 Mo
D	CAST 20 Cr-10 Ni-3 Mo	FORGED Z3 CNP 17-12	CAST Z3 CNP 17-12
E ₁	NONE	24 Cr-12 Ni	24 Cr-12 Ni
E ₂	NONE	20 Cr-10 Ni-3 Mo	20 Cr-10 Ni-3 Mo

LOCATION OF SIDE-DRILLED HOLES*

	MID-THICKNESS	57 mm DEEP	30 mm DEEP	70 mm DEEP	25 mm DEEP	65 mm DEEP
A	1	6	1	5	1	5
B ₁)	2	7	2	8	2	6
B ₂)						
C	3,4	8	3	6	3	7
D	5	9	4	7	4	8
E ₁)	t = 60 mm		t = 90 mm		t = 80 mm	
)						
E ₂)	2 mm Φ		3 mm Φ		3 mm Φ	
)						
	ROOT NOTCHED 3 x 3 mm (9)					3 x 3 mm (9)

* NUMBER GIVEN TO IDENTIFY

The effect of UT-beam configuration is relevant to advanced or alternate UT equipment. A focussed probe proved best because fewer grains were scanned and the UT energy was concentrated into a limited volume of material; this reduced the level of background noise due to diffraction at grain interfaces.

The optimum system was a 1-MHz longitudinal-wave probe at angles of incidence of 45° suitably damped and a focal range optimized for 30-mm and 57-mm depths. Specific probes were developed or selected. Two procedures were emphasized--focussed probes used in immersion and twin probes in contact.

Standard transducers could not detect hole number 9 from the ferritic side using the parameters above relevant to standard transducers. Using these transducers from the cast stainless steel side only hole number 6 could be detected.

Cases II and III in Table 13.5.2 included the complex weldments cited. These weldments contained 3-mm ϕ holes and 3- x 3-mm notches at the weld root:

- In Weldment II, all (holes and notch) detected when the angles refracted in the steel (θ) were $40^\circ < \theta < 50^\circ$.
- If $\theta > 40^\circ$, the variability of amplitude at a given depth was less.
- Only 45° permitted a signal-to-noise ratio of greater than 12 dB (an exception was a signal-to-noise ratio of 9 dB for hole number 2, from the clad surface through the stainless steel base metal.)
- Many spurious signals were observed at $\theta < 30^\circ$.

The preceding results were obtained by changing the refraction angle in 10° steps using a 2.25-MHz 0.75-in. transducer where the reference amplification of 0 dB was established at 6/7 screen height on a 2-mm SDH at 50 mm in a ferritic block.

Weldment III emphasized cast materials with concomitantly higher attenuation in cast stainless steel:

- From the ferritic side at $\theta = 45^\circ$, all except the notch (9) were detected.

- From the cast stainless steel side at $\theta < 60^\circ$, all holes at 25-mm depth were detected; none were detected at 55-mm depth.
- Many spurious signals occurred at $\theta < 30^\circ$.
- Optimum range of angles was $40^\circ < \theta < 45^\circ$.
- The signal-to-noise ratio was very low from the cast stainless steel side.
- The preceding applied to longitudinal waves.

Tomlinson et al.^(13.2.6) examined a trimetallic weld between ferritic and austenitic plates. A J-preparation was used and a manual metal arc process. Thickness was 50 mm. Two 3.0-mm ϕ holes were located, one at the weld C, the other at the ferritic-Inconel interface near the weld root. Both holes were detected with a normal beam with signal-to-noise ratio of at least 12 dB. The holes were detected with 45° L-waves; however, the signal-to-noise ratio was lower. Beam skewing led to errors in location.

The authors^(13.2.6) examined a bimetallic weld consisting of a ferritic plate buttered with 2.25 Cr-1 Mo followed by 316 SS. A metal inert gas process was used leading to fans of grains rather than epitaxially grown dendrites. Three 3-mm ϕ holes were located 1) at the weld, C; 2) at the buttering weld interface near the root, and 3) at the buttering-ferritic plate interface about at mid-thickness (25 mm). The lower holes could not be detected with a normal beam.

13.5.1.2 Stainless Steel Weldments

Kupperman and Reiman^(13.4.5) used a 2.25-MHz broad-band unit in the longitudinal, horizontal shear, and vertical shear modes. While the horizontal shear is of scientific interest, it has found little if any field use. Results will be cited with the preceding caveat concerning general use.

Two 2.4-mm ϕ SDHs were located at 1) the weld centerline in a 50-mm thick section at the midpoint and 2) in the weld close to the base metal about 10 mm from top of weld (304 SS; 308 SS weld). Results for the two holes were as follows:

1. At weld C (a) (normal incidence S&L waves)
 - a. S_H waves - readily detectable; good S/N ratio
 - b. S_V waves - not detected; poor S/N ratio
 - c. L waves - detectable (small signals); fair S/N ratio.
2. Offset hole (b) (45° S&L waves)
 - a. S_H waves - readily detectable; good S/N ratio
 - b. S_V waves - poor detection; poor S/N ratio
 - c. L waves - readily detectable.

L wave propagated parallel to dendrites initially ~3 mm then at 45° to dendrites ~3 mm.

Sandberg^(13.4.2) discusses UT of submerged arc weldments varying in grain size. Most tests used alternate UT; however, some were conducted with conventional UT. The 39-mm thick weldments contained 2.0-mm ϕ or 3.2-mm ϕ SDHs as well as 1 mm x 1 mm or 2 mm notches. Table 13.5.3 contains the data for both conventional and alternate UT. As indicated by the success-to-failure (Y/N) ratios, results were quite sensitive to both testing technique and defect location.

Interestingly, in contrast to other workers, a narrow bandwidth was preferred. The authors^(13.4.2) observed a plus or minus frequency shift depending on initial frequency.

Generally, the 3.2-mm hole could be detected by both L and S waves; however, the S waves varied up to 12 dB in amplitude. The 1-mm x 1-mm notch at the weld root proved difficult but feasible to detect, and the 2-mm x 2-mm was reasonably easy.

Pelseneer and Louis^(13.4.4) examined both cast stainless steel and stainless steel weldments in addition to the bimetallic welds discussed previously. Specimens examined included

1. an austenitic steel casting, essentially ASTM A-351 Grade CF8M.
Dimensions were 188 x 142 x 85.5 mm, and the block contained three 4.8-mm ϕ holes, 20.2-, 41.5-, and 63.13-mm deep.

1. an austenitic steel casting, essentially ASTM A-351 Grade CF8M. Dimensions were 188 x 145 x 85.5 mm, and the block contained three 8 mm ϕ holes, 50.5-, 41.5-, and 63.13-mm deep.

Specimens examined included

less steel weldments in addition to the bimetallic welds discussed previously. Peissner and Louis (13.4.4) examined both cast stainless steel and stain-

lessly 6057.

Generally, the 3.5-mm hole could be detected by both L and S waves; however, the S waves varied up to 15 dB in amplitude. The 1-mm x 1-mm notch at the weld root proved difficult but feasible to detect, and the 2-mm x 2-mm was

on initial frequency.

13.4.3. The authors observed a plus or minus frequency shift depending on initial frequency. Interestingly, in contrast to other workers, a narrow bandwidth was pre-

location.

rates, results were quite sensitive to both testing technique and defect

conventional and alternate UT. As indicated by the success-to-failure (Y/N)

well as 1 cm x 1 mm or 2 mm notches. Table 13.4.3 contains the data for both

front UT. The 34-mm thick weldments contained 5.0-mm ϕ or 3.5-mm ϕ SDHS as

size. Most tests used alternate UT; however, some were conducted with conven-

13.4.2. Sanders discussed UT of submerged arc weldments varying in grain

directions ~3 mm.

L wave propagated parallel to boundaries initially ~3 mm then at 45° to

c. L waves - readily detectable.

b. S waves - poor detection; poor S/N ratio

a. S waves - readily detectable; good S/N ratio

2. Offset hole (b) (45° S&L waves)

c. L waves - detectable (small signals); fair S/N ratio

b. S waves - not detectable; poor S/N ratio

a. S waves - readily detectable; good S/N ratio

1. At weld C (a) (normal incidence S&L waves)

TABLE 13.5.3. Result Digest-Ultrasonic Scanning of Austenitic Stainless Steel Weldments Containing Artificial Defects

RESULT DIGEST SHOWS DEFECT LOCATION AND SIZE, SCANNING DIRECTION AND IF THE MACHINED DEFECT IS POSSIBLE OR NOT TO DETECT FOR EACH PROBE. A YES REPRESENTS AN AVERAGE S/N GREATER THAN 6 dB DETECTED IN ALL OR ALMOST ALL SCANS.		SDH NO. 3 HAZ Ø 3.2 mm	SDH NO. 2 WELD CENTER Ø 3.2 mm	SDH NO. 1 HAZ Ø 3.2 mm	SDH SWRI HAZ Ø 2.0 mm	NOTCH WELDROOT DEPTH 1.0 mm	NOTCH WELDROOT DEPTH 2.0 mm	NOTCH WELD - CROWNS HAZ CAP TO 1.0 mm	NOTCH WELD - CROWN/WELD CENTER TO 2.0 mm	
PAN V 395 2.25 MHz	FLAT 45° T IMMERSSION	YES	YES	NO	—	NO	YES	YES	NO	4/7*
PAN V 395 2.25 MHz	FOCUSED 45° T IMMERSSION	YES	YES	YES	—	NO	YES	YES	NO	5/7
PAN V 392 1.0 MHz	FLAT 45° T IMMERSSION	NO	NO	NO	—	NO	NO	NO	NO	0/7
PAN V 392 1.0 MHz	FOCUSED 45° T IMMERSSION	—	—	NO	—	NO	—	—	—	0/2
PAN V 389 0.5 MHz	FLAT 45° T IMMERSSION	NO	NO	NO	—	NO	—	NO	NO	0/6
PAN A 403-S 2.25 MHz	FLAT 45° T CONTACT	YES	NO	NO	—	YES	YES	YES	NO	4/7
PAN A 401-S 1.0 MHz	FLAT 45° T CONTACT	YES	NO	NO	—	YES	YES	NO	NO	3/7
PAN V 395 2.25 MHz	FLAT 55° T IMMERSSION	YES	YES	YES	—	NO	YES	—	—	4/5
PAN V 395 2.25 MHz	FLAT 70° T IMMERSSION	YES	YES	YES	—	NO	YES	—	—	2/3
PAN V 395 2.25 MHz	FOCUSED 70° T IMMERSSION	YES	—	YES	—	NO	—	—	—	2/4
PAN V 392 1.0 MHz	FLAT 70° T IMMERSSION	YES	NO	YES	—	—	NO	—	—	4/5
RTD 76-38 2.0 MHz	QUASI FOCUSED 45° L - CONTACT	YES	YES	YES	—	NO	NO	YES	YES	5/7
RTD 76-40 2.0 MHz	QUASI FOCUSED 50° L - CONTACT	YES	YES	YES	—	NO	YES	NO	NO	4/7
RTD 76-45 2.0 MHz	QUASI FOCUSED 70° L - CONTACT	—	YES	—	—	—	—	—	—	1/1
SWRI 1.5 MHz	FLAT 45° T CONTACT	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	FLAT 60° T CONTACT	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	FOCUSED 45° T IMMERSSION	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	FOCUSED 60° T IMMERSSION	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	FOCUSED 45° L IMMERSSION	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	PHASED ARRAY 45° L-IMMERSSION	—	—	—	YES	YES	—	—	—	2/2
SWRI 1.5 MHz	PHASED ARRAY 45° T-IMMERSSION	—	—	—	NO	NO	—	—	—	0/2
SWRI 1.5 MHz	PHASED ARRAY 60° T-IMMERSSION	—	—	—	NO	NO	—	—	—	0/2
		*10/12	7/12	7/8	1/8	1/8	7/10	4/8	1/8	

*RATIO OF $\frac{Y}{Y+N}$ (SUCCESS FACTOR)

2. another austenitic steel casting similar to number 1 having dimensions of 228 x 100 x 70 mm and containing a fairly large natural defect.
3. a butt weld joining a 316 SS pipe (36-mm wall) to a cast CF8M pipe (34-mm wall). No information was given concerning the welding conditions. There were two 3.4-mm ϕ holes, one in the middle of the weld and the other in the CF8M near the HAZ and 10 mm below the top surface.

Both conventional and alternate UT techniques were used to examine the test blocks. Table 13.5.4 contains data for number 1 above using both conventional and alternate UT. With standard shear wave no holes were detected.

Block 2, containing a natural defect (Figure 13.5.1), was examined with RT as well as a 2-MHz normal probe. Sizing used the 6-dB drop method. As noted the flaw was detectable and sizing was nominally the same.

The pipe weldment (No. 3) was examined through the casting, through the weld, and through the extrusion. Results for conventional UT are summarized as

<u>Shear Mode (45°, 60°, 70°)</u>	<u>Signal</u>	<u>Hole 1 Weld C</u>	<u>Hole 2</u>
Through casting	None		
Through extrusion	Yes	Yes (all angles)	No
Through weld	Limited	--	--
<u>Longitudinal Mode (45°, 60°, 70°)</u>		<u>Hole 1 Weld C</u>	<u>Hole 2</u>
Through casting		No	No
Through extrusion		Yes (60°, 70°)	No
Through weld		--	--

TABLE 13.5.4. Tests on a Cast Austenitic Steel Block; Comparison Between Standard and Specially Made Probes

CAST AUSTENITIC STEEL A-351/CF 8 M		TRANSVERSE WAVES	LONGITUDINAL WAVES TWIN CRYSTAL	V45LF - 1	V45LF - 2	V45LF - SE - 1	V45LF - SE - 2	TRANSVERSE WAVES	LONGITUDINAL WAVES TWIN CRYSTAL	V60LF - 1	V60LF - 3	V60LF - 4	V60LF - SE - 1	V60LF - SE - 2	TRANSVERSE WAVES	LONGITUDINAL WAVES TWIN CRYSTAL	V70LF - 3	V70LF - SE - 2
X IN FERRITIC STEEL (DEGREES)		45.9	45.0	45.8	47.3	45.0	44.0	59.0	53.1	55.7	53.8	58.5	56.9	56.9	67.8	70.0	70.5	64.8
1st HOLE: 20.2 mm DEEP	AMPLI. LEVEL A1 (dB)	—	40	32	40	56	52	—	46	38	48	40	54	60	—	—	56	56
	ECHO HEIGHT (% SCREEN HEIGHT)	—	90	90	90	80	90	—	80	90	95	80	90	80	—	—	90	95
	GRASS LEVEL (% SCREEN HEIGHT)	—	5	5	10	40	25	—	10	15	15	10	25	15	—	—	25	20
	CORRESP. X (DEGREES)	—	42.8	45.7	47.7	53	54.0	—	51.3	54.5	56.7	56.7	59.2	60.6	—	—	59.2	61.9
	CORRESP. VELOCITY (m/s)	—	5670	5890	5940	6660	6870	—	5760	5810	6110	5780	6050	6140	—	—	5380	5750
2nd HOLE: 41.5 mm DEEP	AMPLI. LEVEL A2 (dB)	—	52	42	44	52	52	—	58	50	60	56	56	60	—	—	—	60
	ECHO HEIGHT WITH A2 AMPLIF. LEVEL (% SCREEN HEIGHT)	—	90	95	85	90	90	—	80	80	80	80	80	85	—	—	—	80
	GRASS LEVEL (% SCREEN HEIGHT)	—	15	15	10	15	10	—	20	30	20	25	25	15	—	—	—	30
	ECHO HEIGHT WITH A1 AMPLIF. LEVEL (% SCREEN HEIGHT)	—	15	25	50	>100	90	—	15	10	15	10	60	85	—	—	—	50
	CORRESP. X (DEGREES)	—	47.9	48.6	46.7	47.6	47.3	—	48.6	54.4	53.0	55.6	53.7	53.5	—	—	—	53.7
3rd HOLE: 63.125 mm DEEP	CORRESP. VELOCITY (m/s)	—	6190	6170	5840	6160	6240	—	5530	5800	5840	5710	5680	5660	—	—	—	5260
	AMPLI. LEVEL A3 (dB)	—	—	48	64	58	58	—	—	52	66	58	—	—	—	—	—	—
	ECHO HEIGHT WITH A3 AMPLIF. LEVEL (% SCREEN HEIGHT)	—	—	90	85	85	90	—	—	90	90	80	—	—	—	—	—	—
	GRASS LEVEL (% SCREEN HEIGHT)	—	—	30	20	25	30	—	—	15	25	20	—	—	—	—	—	—
	ECHO HEIGHT WITH A2 AMPLIF. LEVEL (% SCREEN HEIGHT)	—	—	40	10	50	45	—	—	70	45	60	—	—	—	—	—	—
	ECHO HEIGHT WITH A1 AMPLIF. LEVEL (% SCREEN HEIGHT)	—	—	10	5	75	45	—	—	10	5	5	—	—	—	—	—	—
	CORRESP. X (DEGREES)	—	—	48.2	48.0	50	44.8	—	—	51.4	52.8	54.0	—	—	—	—	—	—
	CORRESP. VELOCITY (m/s)	—	—	6140	5970	6310	5990	—	—	5580	5820	5600	—	—	—	—	—	—

FIGURE 13.5.1. Shape of a Natural Defect in a Cast Austenitic Steel Block as Estimated by Radiography, Standard Normal Probe, and V45LF-SE-1 Probe

Gray et al.^(13.4.3) emphasized beam skewing and convergence or divergence in their study; however, they did measure gain settings and signal-to-noise as a byproduct. Figure 13.5.2 presents the data for specimens 1 and 2. Measurements were made in terms of gain setting for centerline and fusion-line holes (1.5 mm ϕ). As can be seen there is a substantial spread in gain setting.

The authors^(13.4.3) examined both electron beam and tungsten inert gas welds containing SDHs, probably 1.5 mm ϕ . Figure 13.5.3 presents data on what amounts to DAC curves.

Neumann et al.^(13.4.7) examined the reliability of different ultrasonic techniques using quantitative statistical methods. An austenitic test weld, containing a large number of intentionally produced natural flaws, such as pores, slag, lack-of-fusion, etc., was examined. Some 27 longitudinal flaws were evaluated from both sides of the weld. Radiography was used for sizing and location. Four techniques were considered: 1) shear-wave single transducer; 2) longitudinal-wave single transducer; 3) longitudinal-wave focussed transducer; and 4) longitudinal wave with transmitter and receiver probes. All were at 60° and 3-mm wave length. Results are presented for the four cases,

FIGURE 13.5.2. Amplitude Results for Downhand Manual Metal Arc (MMA) Welds When Examined from the Crown

two conventional and two alternate, in Figure 13.5.4 in terms of cumulative echo height distributions of flaw indications (P_{detect}) and scatter (P_{scatter}) in terms of a probability diagram as a function of relative gain setting ΔV (dB) where $\Delta V = 0$ dB corresponds to the sensitivity setting for a 3-mm flat-bottomed hole (FBH) on 80% full screen.

The P_{detect} curves show cumulative echo height measured in terms of gain setting ΔV . The P_{scatter} curves indicate scattering indications. If P_{scatter} exceeds the reference level, it is considered a flaw signal.

In coarse-grained material, echo height of scatter indications may control. The noise level, $\Delta V = R_1$, is defined as the gain setting where the cumulative echo height of scatter is 1%. This means the noise level is that level exceeded by 1% of all scatter indications where 99% of all scatter indications fall below that level. This method permits signal-to-noise ratio to be determined objectively and reproducibly with an error of no more than 2 dB. This

FIGURE 13.5.3. Amplitude Results for Two Narrow Gap Welds When Examined from the Crown

technique yields maximum flaw detection probabilities as a gain setting corresponding to the noise limit R_1 for conventional probes of 59% for L-wave single transducers. Signal/noise levels are 7 dB for L-wave and 2.5 dB for S-wave.

Weldments in thick sections of cast austenitic alloys typical of PWR primary system piping combine the problems of beam attenuation and skewing discussed in this chapter. An extensive study by Pade and Enrietto^(13.5.1) provides some answers to the question—"How well can centrifugally cast austenitic stainless steel be examined with UT?"

Samples were from a single section of cast CF 8A SS, 32-in. OD x 27-in. ID x 22-ft long. Welds were made in sections of the pipe using TIG and SAW in downhand, overhead and vertical positions. Fatigue cracks were introduced on both inner and outer surfaces.

FIGURE 13.5.4. Cumulative Echo Height Distributions of Flaw Indications P_{Detect} and Scatter Indications P_{Scat} as Functions of Relative Gain Setting ΔV from an Austenitic Test Weld. T = transverse wave single transducer-; L = longitudinal wave single transducer-; FL = longitudinal wave focus-; and SEL = transmitter-Receiver Probe; $\alpha = 60^\circ$; $\lambda \approx 3 \text{ mm}$

Three teams were used with the criterion one of properly characterizing a pair consisting of a cracked and a control specimen. Correct analyses led to another pair with a shallower (5%) crack. Failure led to a pair with one containing a deeper (5%) crack. A series of evaluation criteria were used in the assessment; namely,

- Could the test systems be calibrated per the test procedure?

- Could the crack be detected from either one or both surfaces?
- Determine the maximum crack length using 50% DAC.
- Qualitatively analyze the signal-to-noise ratio for the cracks.
- Determine the maximum signal (when assessing the effect of test frequency and transducer type and size).

An interesting study that was a precursor of the sample examination consisted of testing five different UT instruments in combination with four different probes using the relative ability to detect 5%, 15% and 25% cracks as criteria. Table 13.5.5 summarizes this program.

The following significant conclusions were derived from this study:

- The Krautkramer probe was considered the best; however, its large size makes field testing of pipe welds difficult.
- There was pronounced variability among the various instruments.
- Glycerine was a better couplant than Exoson 30.
- Only one (of three) Vincotte probes was considered accurate; this was the 1.0 MHz 45°.
- The Automation Industries probe was equal to or better than the other special probes.

TABLE 13.5.5. Ability to Detect 5, 15, 25% Cracks

Instrument	Probe			
	K.K. 0.5 MHz 45° L-wave	Vincotte, 1 MHz 45 x 20 mm, 45° L-wave	Vincotte, 1 MHz 10 x 10 mm, 45° L-wave	Auto Ind. 1 MHz 40° L-wave
Auto Ind UT	Faulty Inst.	5, 15, 25%	5, 15, 25%	All
Sperry UM 721	All	15, 25%	15, 25%	Not tested
K.K. ISSIP-II	All	15, 25%	5, 15%	15, 25%
KK USM-2	Could not calibrate	15, 25%	15%	Not tested
Sonics - MARK-I	All	15, 25%	15%	All

The data contained were examined in the context of the ratios used in Chapter 3 (R_I , R_{II} , R_{III} , R). Table 13.5.6 contains these analyses. The results are surprisingly high in terms of reliabilities. In my estimation the repetitive nature of testing might account for the values; however, this is only an inference.

The general conclusions in the report^(13.5.1) are paraphrased:

- Centrifugally cast stainless steel weldments are more difficult to UT than carbon steel weldments; however, they can be examined. Most problems were due to geometry.
- Statistical analyses (268 points) indicated detection reliabilities with respect to crack depths of 5-80%, 10-86%, 15-87%, 20-94%, 25-100%, 30-100%.
- Elongated grains typical of stainless steel weldments pose UT problems; however, the consistency of structure permits corrective measures. Some key test parameters are as follows:
 - Longitudinal wave is preferable because of lower noise and attenuation. Preferred incident angles are 9° for 1 MHz and 7° for 2.25 MHz.
 - Training is more significant than experience.
 - O.D. cracks are more detectable than I.D. cracks.
 - Crack orientation influences UT detection.
 - Matching of test equipment to search unit is critical.
 - Larger transducers yield better results.
 - As crack depth increases, detectability increases.
 - Pitch-catch has promise, provided crossover points are optimized.

13.5.2 Alternate UT Techniques

The following is a logical extension of detection with standard probes developed in the previous section. With regard to weldment I in Table 13.5.2,

TABLE 13.5.6. Analysis of Operator Accuracy by Crack Depth

Operator	Flaw Size					
	5%	10%	15%	20%	25%	30%
1 R _I	12/15 = 80%	22/23 = 96%	24/25 = 96%	16/17 = 94%	8/8 = 100%	1/1 = 100%
R _{II}	9/15 = 60%	21/23 = 91%	23/25 = 92%	15/17 = 88%	8/8 = 100%	1/1 = 100%
R _{III}	12/9 = 1.33	22/3 = 7.33	24/8 = 3.0	16/6 = 2.67	8/2 = 4.0	1/0 = ∞
R	12/21 = 57%	22/25 = 88%	24/32 = 75%	16/22 = 73%	8/10 = 80%	1/1 = 100%
2 R _I	13/15 = 87%	17/23 = 74%	17/25 = 68%	16/17 = 94%	8/8 = 100%	1/1 = 100%
R _{II}	11/15 = 73%	11/23 = 48%	9/25 = 36%	15/17 = 88%	8/8 = 100%	1/1 = 100%
R _{III}	13/2 = 6.5	17/7 = 2.43	17/12 = 1.42	16/1 = 16.0	8/0 = ∞	1/0 = ∞
R	13/15 = 87%	17/24 = 71%	17/29 = 59%	16/17 = 94%	8/8 = 100%	1/1 = 100%
3 R _I	11/15 = 73%	20/23 = 87%	24/25 = 96%	16/17 = 94%	7/7 = 100%	1/1 = 100%
R _{II}	7/15 = 46.7%	17/23 = 74%	23/25 = 92%	15/17 = 88%	7/7 = 100%	1/1 = 100%
R _{III}	11/9 = 1.22	20/6 = 3.33	24/9 = 2.67	16/3 = 5.33	7/0 = ∞	1/0 = ∞
R	11/20 = 55%	20/26 = 77%	24/33 = 73%	16/19 = 84%	7/7 = 100%	1/1 = 100%

$$R_I = \frac{F}{F + U} \times 100$$

$$R_{II} = \frac{F - U}{F + U} \times 100$$

$$R_{III} = \frac{F}{f + U}$$

$$R = \frac{F}{F + U + f} \times 100$$

NOTES: F = Found
U = Unfound
f = False Indication

the detection of holes at 30-mm depth was better from the cast SS side because reflectors can be detected without interference from the shear waves which are heavily attenuated.

The deeper (57-mm) holes could be detected better from the ferritic side using twin contact probes. If focussed immersion probes are used, the examination from the cast stainless steel side is preferred. These procedures are better than standard probes due to suppression of spurious echoes and reduction of background noise.

Focused probes initially yielded spurious signals originating at the water-steel interface; however, all holes were detected. The twin contact probes have better resolution than focussed probes; however, hole number 9 could not be detected. Not too surprisingly, attenuation was minimized at 45° to the fiber axis. The basis for the lower (1-MHz) frequency selected was reduction of attenuation and anisotropy of attenuation. The special probes had advantages of increasing the signal-to-noise ratio and the suppressing of spurious echoes.

13.5.2.1 Stainless Steel Weldments

The tests of Sandberg^(13.4.2) are summarized in Table 13.5.1. Focused probes were found to help in detecting the weld root notch compared to flat transducers. As indicated focussing enhances the reliability of detection.

The report conclusions are of interest because of the negative aspects. Extensive use was cited of signal processing to separate metallurgical noise from flaw indications. The tests included three-dimensional imaging, holographic imaging, etc. The significant conclusion was that signal processing did not help significantly in improving detectability of machined defects in welded specimens. The conclusion should be considered in the correct perspective. No confirmatory data were included in the report, and no information was given as to the time frame of the work. It could be middle 1970s or very new.

Pelseneer and Louis^(13.4.4) examined the stainless blocks cited previously in Section 13.5.1.2. Data for number 1 was given in Table 13.5.4. As noted

twin crystal L-waves with some focussing could detect the first two but not the third hole. Special V probes were able to detect all three holes with a good signal-to-noise ratio.

Figure 13.5.1 illustrates results of detection and sizing of a natural flaw in Block No. 2 using a special V probe.

Examination of weldment No. 3 cited by Pelseneer and Louis^(13.4.4) was much more successful using the special V probe. They were able to detect the hole at the weld centerline, and the hole in the casting was detected from the casting side with both L and S waves. From the cast side only L-wave probe V 70 LF SE-2 was successful.

The above probes were short pulse length 2 MHz and small size.

Silk et al.^(13.2.5) used the time domain approach previously discussed in Chapter 5 for the sizing of a fatigue crack in a 316 SS weldment. The ultrasonic estimate of the crack profile is compared to the actual profile in Figure 13.5.5.

FIGURE 13.5.5. A Comparison Between the Actual Defect Depth and the Corresponding Ultrasonic Estimates for a Fatigue Crack in Austenitic Stainless Steel Weld

Neumann et al. (13.4.7) examined an austenitic weld using statistical methods for evaluation. This is described in 13.5.1.2. The advanced procedures are illustrated in Figure 13.5.4. The two advanced procedures gave the following results:

	<u>Probability of Detection</u>	<u>Signal-To- Noise Ratio</u>
Transmitter-Receiver (L-wave)	87%	11 dB
Focussed Probe (L-wave)	79%	11 dB

FIGURE 13.5.5. A comparison between the actual defect depth and the corresponding Ultrasonic Distance Estimates for a fatigue crack in austenitic stainless steel weld.

The ultimate test of any analytic model is its validation with experimental data. In the case of NDE reliability, the values will be sensitive to many factors such testing technique, materials, surface conditions, types and locations of defects. For example, the results with fabrication defects in weldments will differ from operationally induced defects such as fatigue or stress corrosion cracks.

The important defect from a safety point of view is one that grows with time, not some forms of fabrication defects that do not change in size throughout the life of the component. Obvious examples of stable defects include slag and porosity in most instances as well as deeply embedded crack-like defects. Some idea of defects of safety significance in piping, namely, those that grow, can be seen in Table 13.6.1 taken from a paper by Bush.^(13.6.1) Figure 13.6.1 presents the same data in terms of growing cracks and growing cracks that leak. Unfortunately, the quantity of quantitative NDE data on growing defects such as IGSCC or corrosion fatigue is quite limited. This is particularly true for cracks initiating on the inner surface of piping such as most of those in Table 13.6.1. Table 13.6.2 summarizes several sources of data relevant to detecting various types of defects by a variety of NDE techniques. These data will be discussed in later sections of this chapter.

13.6.1 Fabrication-Induced Defects

Substantial NDE work has been done with regard to the detecting, locating, and sizing of fabrication defects such as porosity, slag, lack-of-fusion, lack-of-penetration and cracking. Such defects are typical to ferritic weldments.

Buken and Krachter^(13.6.2) compared RT and UT with regard to the sizing of defects such as porosity, slag and a combination of the two. The pattern (Figure 5.6.2) is a "shotgun" with the UT generally predicting smaller sizes than RT. The values are in terms of measured rather than absolute values and would be of minimal use in constructing a probability density function.

Kato et al.^(13.6.3, 13.6.4) examined the ability of RT and UT to size defects in both length and depth directions. The earlier study^(13.6.4)

TABLE 13.6.1. Incidence of Major Cracking or Leaking in Nuclear Components(a)

Type of Defect	Type of Plant	1965	1966	1967	1968	1969	1970	1971	1972	1973	1974	1975	1976	1977	1978	1979	TOTAL
Stress Corrosion	BWR	1	6	13	8	8	3	1	1		15	22	14	23	8	2	109
	PWR												2				2
Thermal Fatigue	BWR								1	4	6	18	19	6			54
	PWR						1									14	15
Design-Dynamic Load	BWR																0
	PWR						1	1									2
Construction Defects/Errors	BWR								2		2		5		4	2	15
	PWR																0
Water Hammer (Line Failure)	BWR										1			2			0
	PWR																3
Fatigue-Vibrational	BWR															1	1
	PWR											2				3	5
Fatigue	BWR												1			2	3
	PWR									1					1		2
Erosion/Corrosion	BWR									1		2		2	2	1	8
	PWR																0
Corrosion/Fatigue	BWR																0
	PWR																0
Unknown Cause	BWR														2		2
	PWR														1	1	2
TOTAL		1	6	13	0	0	5	2	4	6	24	44	41	33	18	26	222

(a) Piping >4 in. dia unless otherwise noted as to component 191 BWR; 31 PWR.

* NUMBERS INDICATE NUMBER OF REACTORS IN WHICH INCIDENTS OCCURED

FIGURE 13.6.1. Incidents of Major Cracking or Leaking in Nuclear Components (Primarily Piping)

TABLE 13.6.2. Sources of Nondestructive Examination (NDE) Data Relevant to Reliability of Detection of Both Fabrication and Operational Defects

Fabrication-Induced Defects

Thickness and Materials Similar to Nuclear Piping

1. Buken and Krachter(13.6.2): ferritic steel; thickness variable; porosity and slag; UT, RT.
2. Kato et al.(13.6.3, 13.6.4): ferritic steel; thickness 16-60 mm; root cracks, internal cracks, lack-of-penetration, slag; UT, RT.
3. Fujimori, T.(13.6.5(a)) and Y. Ishii and Fujimori, T.(13.6.5(b)): ferritic steel; thickness to 25 mm; brittle cracks; UT.
4. Isono, E.(13.6.6): ferritic steel; thickness 20-50 mm; lack-of-penetration, lack-of-fusion, cracks; RT, UT.
5. Kishigami, M., et al.(13.6.7): ferritic steels; thickness 12-34 mm; cracks, incomplete penetration, lack-of-fusion, slag, porosity; UT, RT.
6. Papke, W. H. and H. A. Stelling from H. J. Meyer(13.6.8): ferritic steel; thickness 10-50 mm; cracks, porosity, slag, lack-of-fusion; RT, UT.
7. Forli, O.(13.6.9): ferritic steel; thickness 10-26 mm; porosity, lack-of-fusion; lack-of-root penetration; RT, UT.

Thickness Greater Than Nuclear Piping, Materials Similar

1. PVRC and PISC programs (see Chapter 3): ferritic steel; thickness 130-270 mm; porosity, slag, cracks, lack-of-fusion; RT, UT.

Operationally Induced Defects on Examination Surface

Materials Similar to Nuclear Piping

1. Caustin, E. L.(4.2.5, 13.6.10): ferritic steel; primarily surface flaws; ET, MT, PT, UT.
2. Herr, J. C.(4.2.7, 13.6.11): ferritic steel; surface flaws.
3. Packman, P. F., et al.(4.2.2, 13.6.12): ferritic steel cylinders; primarily fatigue cracks; ET, MT, PT, UT, RT.

TABLE 13.6.2. (contd)

Materials Different From Nuclear Piping

1. Caustin, E. L.(4.2.5, 13.6.10): aluminum alloys; primarily surface flaws; ET, PT, UT.
2. Lord, R. J.(4.2.8, 13.6.13): titanium alloys; tight cracks, gross cracks, porosity; ET, PT, RT, UT.
3. Packman, P. F.(4.2.2, 13.6.12): aluminum alloys; primarily fatigue cracks; ET, PT, RT, UT.
4. Pettit, P. E. and D. W. Hoepfner(4.2.11, 13.6.14): fatigue cracks; ET, PT, RT, UT.
5. Rummel, W. D. and R. A. Rathke(4.2.1, 13.6.15): aluminum alloys; fatigue cracks; ET, PT, RT, UT.
6. Yee, G. G. W.(4.2.12, 13.6.16): aluminum alloys; fatigue cracks; UT.
7. Lewis, W. H., et al.(13.6.17): aluminum alloys; fatigue cracks; ET, PT, RT, UT.

Opposite Examination Surface

Materials Similar to Nuclear Piping

1. Reinhart, E. R.(4.5.26, 13.6.18): austenitic steel; IGSCC; lack of fusion; UT.
2. Becker, F. L.(13.6.20): ferritic steel; fatigue cracks; UT.

contains regression analyses of more limited data than presented in the later study.(13.6.3) Figures 5.6.5 - 5.6.9 contain the more extensive data. It is apparent that A-scan, vanishing echo, B-scan and RT yield similar and accurate results in length measurements, and B-scan is fairly accurate for measuring flaw depth. Table 5.6.7 reproduced here as Table 13.6.3 indicates the results of the statistical analysis.

Fujimori(13.6.5(a)) and Ishii and Fujimori(13.6.5(b)) reviewed early work in Japan pertinent to the sizing of near-surface and surface flaws. Data are the same or essentially the same as reported by Kato et al.(13.6.4)

TABLE 13.6.3. Statistical Data for Population Tested by Specified Method

	Length Ultrasonic				RT	Height Ultrasonic	
	A-Scope		V.E.	B-Scope Method		A-Scope	B-Scope
	6 dB	10 dB				6 dB	
Sample Numbers	168	389	83	110	208	133	38
Mean Value of Error (mm)	-8.01	0.15	-0.68	1.32	0.84	0.49	-0.68
Variance (mm ²)	119.87	47.97	9.96	12.86	12.12	9.91	6.32
Two-Sided Tolerance (mm)	-39.77	-19.00	-10.20	-9.20	-8.97	--	--
(mm)	+23.75	+19.30	+8.82	+11.84	+10.65	--	--
<u>F-Test Results</u>							
6 dB	--	S	S	S	S		
10 dB	--	--	S	S	S		
V.E.	--	--	--	N	N		
B-Scope	--	--	--	-	N		
RT	--	--	--	-	-		

S = Significant

N = Nonsignificant

Isono^(13.6.6) presented a relatively small portion of a more extensive continuing study on detection and sizing of defects. Table 13.6.4 permits an overview of the general program, including types of defects and manner of generating. Table 13.6.5 compares actual defect sizes measured from the fractured surfaces, the lengths measured by RT, the lengths by UT, the height by UT and the percentage of the original cross section occupied by the defect.

In the RT, there was variability from operator to operator as well as substantial under-prediction of flaw length. The UT data were quite variable from one side to the other. Generally, UT underpredicted length rather than over-predicting it. Measurements of defect height (depth) were insufficient to establish any trends. It would be necessary to examine a much wider spectrum of specimen thickness and flaw characteristics to conclude that definitive trends exist. The UT system used A-scan with DAC and a quasi-3-dimensional display.

Kishigami et al.^(13.6.7) examined a spectrum of construction steel welds using both RT and UT. With regard to UT, the authors raised two questions:

1. Can UT appropriately judge a truly harmful defect?
2. Can inspection and judgment skills be acquired by inspection and training to the level to meet the requirements of the industry?

A frequency of 4 MHz and a special calibration block were used to minimize surface echoes. Materials ranged from 12-34 mm for bridge steel and 10-25 mm for automatic one-sided weldments. Some 1110 m of weldment were examined in the bridge steel with the results given in Table 13.6.6.

It is apparent that RT is quite effective for the more innocuous defects such as slag and porosity.

Several examinations were made of automatic one-sided weldments in 10-25 mm thick steel plates, in thin (12 mm) plates, and in actual structures (tanks) of 10-12 mm thickness. The flaws in these weldments were classified into three categories:

I	small	<	2 x 2 mm flaws; acceptable
II	intermediate	>	2 x 2 mm flaws; conditionally acceptable
III	large	>>	2 x 2 mm flaws; unacceptable

TABLE 13.6.4. Test Specimens and Example of Artificial Defects

GROUP	THICKNESS (mm)	NUMBERS OF TEST PIECE	GROOVE	DEFECTS		DESTRUCTIVE TESTS
				KIND	POSITION	
I	25 & 50	6	✓	LOP	SURFACE	TENSION
II	25	20	X	LOP, CRACK	INTERNAL	TENSION
III	20	15	X	LOP	INTERNAL	FATIGUE
IV	20	2	V	LOP, SLAG	INTERNAL	—

* DEFECT MAKING

(a) CRACK

(b) LACK-OF-PENETRATION

TABLE 13.6.5. Test Results of Defects in Group III Test Pieces

Test Piece Number	Fractured Surface Length x Height (a)	X-Ray Film (b)		Length by UT Method		Height by UT Method		Defect Size (%) (d)	
		X	Y	1A	1B	hA	hB	1xh	Planimeter
P10-1	<33 x 5.5 29 x 4.5	0	0 + BH	22	<u>24</u>	5~10	5~10	4.3	4.0
10-2	<31 x 4.2 30 x 4	0	0	<u>20[6](c)8</u>	24	5~10	10~15	4.0	3.3
10-3	<33 x 4.0 34 x 4	0	0	20	<u>24[6]14</u>	5~10	5~10	4.5	3.7
10-4	<38 x 3.6 33 x 3.5	0	0	<u>6[12]6</u>	18	5~10	5~10	3.9	3.0
10-5	<31 x 3.2 31 x 4	0	3 + BH	<u>22</u>	20	5~10	<5	4.1	2.3
P11-1	<48 x 5.7 41 x 4	43	30	<u>50</u>	24	5~10	5~10	5.5	6.3
11-2	<46 x 5.5 42 x 4	45 + slag	45	<u>46</u>	22[12]8	5~10	10~15	5.6	6.7
11-3	<43 x 5.1 43 x 4	32	26	<u>50</u>	40	5~10	5~10	5.7	6.7
11-4	<43 x 5.0 42 x 4	38	15	<u>48</u>	14[10]10	5~10	5~10	5.6	6.3
11-5	<47 x 5.5 40 x 3.5	53	30	44	<u>50</u>	5~10	5~10	4.7	5.7
P12-1	<83 x 5.4 81 x 4	58	46	<u>88</u>	14[40]30	5~10	5~10	10.8	12.7
12-2	<81 x 5.2 80 x 4	23[21]36	24	<u>82</u>	80	5~10	5~10	10.7	12.0
12-3	<83 x 5.1 82 x 4	81	80	84	<u>86</u>	5~10	5~10	10.8	12.3
12-4	83 x 5.0	82	85	--	14[16]46	--	5~10	10.9	10.0
12-5	?	58	50	<u>82</u>	80	5~10	5~10	--	--

- (a) The upper values show measurements obtained from photography of fractures and the below shows those from test pieces, but different operators.
 (b) Inspectors are X and Y.
 (c) These values are not recorded by values enclosed with brackets [].
 (d) Values obtained by dividing the defect area length x height in lower lines by the original cross sectional area (20 x 150 mm²).
 (e) Suffix A and B denote the scanning side.
 (f) Underline means the indicated length in case of faultless manual operation.

TABLE 13.6.6. Detection of Flaws by RT and UT

Type of Flaw	Cracks	Incomplete Penetration	Lack of Fusion	Slag	Blow Hole
Total flaws	28	28	14	5	755
Detected by RT	26	21	12	5	755
Detected by UT	28	26	10	0	19

In the thicker (10-25 mm) weldments, some obvious differences in interpretation between RT and UT occurred. For example, UT detected 660 defects of which 245 (37%) were considered unacceptable (III). RT denoted 208 (32%) unacceptable. It is of interest to compare flaws deemed unacceptable by one technique with the other technique. For example, when the 208 flaws designated unacceptable by RT were examined by UT, 16 were classified acceptable (I), 46 (II), 146 (III) with both II and III considered unacceptable. The 16 (I) flaws were defined as 3 incomplete penetration, 11 slag inclusions, or lack-of-fusion and 2 other. In the case of the 53 (I) flaws, defined as acceptable by RT but not by UT, they were broken down into 42 (III) and 11 (II) with the 42 made up of 15 slag inclusions or LOF, 2 blow holes and 25 other.

In the thinner plates, cracks were the predominant mode for unacceptable flaws. Defective back beading was a significant factor for unacceptable welds in these thin sections.

Meyer^(13.6.8) reported work of Papke and Stelling comparing the probability of detection of the same flaws by RT and UT. Their information is duplicated in Figure 13.6.2. As thickness increases the probability of detection decreases with RT. It generally increases with UT; however, centerline porosity represents a special case where UT goes through a maximum at 20-30 mm.

Forli^(13.6.9) reported on the initial phase of a continuing program whose purpose is a comparison of the relative reliabilities of radiography and ultrasonics. Ultimately, 250 m of weldment (butt) containing various defect types are to be examined. The current program was limited to 70 m of mild steel plate containing I, Y or V weld preparations then welded by submerged arc,

FIGURE 13.6.2. Probability of Flaw Detection by Radiographic and Ultrasonic Inspection

manual metal arc, or gas metal arc. Thicknesses were 10–26 mm (0.4–1.0 in.), typical of much piping. Half (35 m) of the weldment was destructively examined to confirm the defect population.

Intentional defects in the 35 m of weldment were

Porosity (A)	2
Lack-of-fusion single (C)	10
Combined with lack-of-root penetration (CD)	12
Lack-of-root penetration (D)	<u>36</u>
Total	60

Several (28) of the class C and D defects were accompanied by slag inclusions. The preceding defects were confirmed by destructive examination. In addition, some porosity clusters and slag inclusions were detected by RT but not found during the destructive examination.

In the context of fracture mechanics significance, only the lack-of-root penetration and fusion defects are considered significant.

Twelve UT operators differing in experience participated in the round robin. There were two RT interpreters. The best that can be said for the UT results over the spectrum of flaw types and sizes is that the data are in a shotgun pattern over the range of defect heights of 1-10 mm. Signal amplitudes ranged from 10-200%.

Regression lines were fitted to the various types of defects, lack-of-root penetration (D), lack-of-fusion (C/CD) and various (C/CD/D). While 95% confidence limits were fitted, the data had a major spread.

The prediction of types of defects versus the actual type of defect is significant. Table 13.6.7 illustrates the differences between actual and predicted types by both RT and UT.

Generally, the uncertainties in both RT and UT were substantial. For example, uncertainties at the 95% confidence level were a factor of 4 for the single operator and a factor of 2 for several operators. Substantial spreads existed for the RT data.

TABLE 13.6.7. Fraction of Defect Types Estimated by NDE for Different True Defect Types (IIW nomenclature)

	True Defect Type	Defect Type Estimated by Radiography						Defect Type Estimated by Ultrasonics				
		A	B	C	CD	D	E	A	B	C	CD	D
Porosity	A	0.5		0.5					0.2	0.2	0.2	0.4
Lack-of-fusion	C	0.2	0.2	0.4		0.2		0.04	0.30	0.31	0.14	0.22
L-O-F and L-O-P	CD		0.1	0.3	0.0	0.6		0.01	0.13	0.22	0.07	0.57
Lack-of-root penetration	D	0.0	0.1	0.1		0.8	0.0		0.14	0.11	0.08	0.67

The problems of flaw detection by both RT and UT are apparent in both the PVRC and PISC programs. The plates ranged from 13- to 27-cm thick so results are not representative of piping wall thicknesses. Examination of the results in Chapter 3 confirms the difficulties in detection by both RT and UT.

13.6.2 Operationally Induced Defects on Examination Surface

Caustin^(13.6.10) used MT, PT, and UT for fatigue cracks. Results with PT and MT were poor at crack depths below 0.010 in. and consistently good above.

Herr^(13.6.11) used MT to detect simulated fatigue cracks in steel with results similar to those of Caustin. Below 0.010-in. depth, all were missed. Above 0.010 in., the record was poorer than with PT with regard to consistency of detection. The same specimens were examined with Delta scan UT.

Table 13.6.8 cites the probability of detection.

Packman et al.^(13.6.12) reported extensive data on detection of fatigue cracks in both aluminum and steel. Laboratory and production NDE were reported in Tables 4.2.3 and 4.2.4. Samples were 3-in. dia cylinders with 0.25-in. wall. These dimensions are comparable to much Class 2 and 3 piping. Crack lengths but not depths were cited. Table 4.2.3 is duplicated as Table 13.6.9. Note that UT yielded more consistent results than PT, MT, or RT.

Pettit and Hoeppner^(13.6.14) reported on the detection of fatigue cracks in aluminum alloys before and after proof testing at 90% yield strength. The best techniques were PT and surface wave UT. The poorest results were with RT.

TABLE 13.6.8. Composite Results of Delta Scan Inspection Evaluations by Simulated Flaw Size Group

<u>Flaw Surface Length (inches)</u>	<u>Number of Trials</u>	<u>Number of Misses</u>	<u>Detection Probability</u>	<u>Detection Probability 95% Confidence</u>
Less than 0.075	78	23	0.71	0.50
0.076 to 0.150	92	5	0.95	0.88
0.151 to 0.225	63	2	0.97	0.90
Greater than 0.225	<u>58</u>	<u>1</u>	0.98	0.90
	291	31		

TABLE 13.6.9. Sensitivity of Detection of Surface Flaws in Aluminum and Steel Cylinders (Table 4.2.2)

Actual Crack Range (2C) (inches)	Aluminum			Steel			
	PT	UT	RT	PT	UT	MT	RT
No crack	0.9333	0.8667	0.9333	0.8889	0.7778	0.7778	
0.000-0.050	0.0667	0.1333	0.0667	0.1111	0.2222	0.2222	0.3900
0.051-0.100	0.1538	0.4615	0.0000	0.4000	0.4000	0.6000	0.0000
0.101-0.150	0.2917	0.6250	0.0417	0.3333	0.8000	0.9333	0.0000
0.151-0.200	0.3636	0.5000	0.0000	0.3000	0.9000	0.9333	0.0000
0.201-0.250	0.8571	0.8571	0.0000	0.6000	1.0000	0.9000	0.0000
0.251-0.300	1.0000	1.0000	1.0000	0.7978	1.0000	0.8889	0.2222
0.301-0.350	1.0000	1.0000	0.1111	0.6250	0.8750	1.0000	0.1250
0.351-0.400	1.0000	1.0000	0.0000	1.0000	1.0000	1.0000	0.0000
0.401-0.450	1.0000	1.0000	0.2500	1.0000	1.0000	1.0000	0.0000
0.451-0.500	1.0000	1.0000	0.8333	1.0000	1.0000	1.0000	0.5000

Rummel et al.^(13.6.15) reported on studies similar to those of Packman et al.^(13.6.12) and Pettit and Hoepfner^(13.6.14). Surface finish was a significant parameter. Chemically milled surfaces improved detection with PT, RT and ET, but not UT, as noted in Table 4.2.2. Trends were similar to those cited previously for ET, PT, UT and RT as can be seen in Figures 4.2.1 through 4.2.8.

Yee et al.^(13.6.16) conducted extensive statistical analyses of NDE data for fatigue cracks in aluminum alloys. Some 34,000 measurements were available; even so there were several gaps in the data. For the 50% detection probability at the 95% lower confidence level, a crack length of 0.2 cm is detected with surface waves UT.

Lewis et al.^(13.6.17) reported on one of the most extensive and statistically significant studies published. This study on components typical of aluminum air frames was established to confirm the Air Force premise that their reliability criteria of 90% probability of detection at a 95% level of confidence was valid. Unfortunately, the study tended to confirm that 50% probability of detection with 95% confidence for 0.5 in. crack sizes was difficult to

achieve. An example can be seen for one case, that of UT, from a choice of ET, PT, RT, UT where Figure 13.6.3 contains the data. As can be seen, the lower bound 95% confidence values are quite low.

FIGURE 13.6.3. Probability of Detection Versus Fatigue Crack Radial Length, Ultrasonic Shear-Wave Scans Around Countersunk Fasteners, Skin and Stringer Wing Assembly.

Statistical analysis of the data are given in Table 13.6.10. Three parameters, flaw size, NDE method, and technician, were examined. The first two parameters, as anticipated, played a significant role. The operator (technician) in one instance played a significant role. In others (field installations) differences were limited.

A cross-section of the significant conclusions follow. One aspect of this study was the unforeseen results. There is an extensive followup study underway to resolve questions raised. (13.6.17)

- A distinctly higher level of flaw detection success was achieved at one exceptional installation, a depot, especially with the eddy-current, bolt-hole method. This demonstrated that considerably better performance levels than those generally exhibited are possible. The superior performance at this one installation is attributed solely to individual proficiency. The attributes which produced the superior performance and which can serve as a model in efforts to upgrade overall NDE performance are believed to be the following:
 1. NDE operations were conducted within an organization which is exclusively dedicated to nondestructive inspection.
 2. A staff of skilled personnel was acquired by a selective process.
 3. A form of certification-recertification was performed at periodic intervals.
- The major variation in inspection results was found among the individual technicians themselves. There are vast differences in the performance levels of individuals, as evidenced in comparisons of curves plotted for the upper ten percent of technicians with the mean curves for all technicians. The factors which might be expected to have a strong direct scoring on individual NDE performance, such as: formal education, technician age, classification, skill level, NDE experience and NDE training, were evaluated and found to have only minimal influence on performance levels. The primary source of

TABLE 13.6.10. Tests for Significance of Variables Conducted Early in the Program from 1 Depot and 3 Field Installations. The "F" ratio is that value which reflects variance due to randomness.

Test Parameter	Depot NDI (Variance Ratio)	Significance Value ("F" Ratio)	Comments	Field NDI (Variance Ratio)	Significance Value ("F" Ratio)	Comments
Flaw Size	15.116	2.6049	Highly Significant	21.726	2.6049	Highly Significant
NDE Method	10.636	2.2141	Highly Significant	26.857	2.2141	Highly Significant
Technician	2.8364	1.9384	Significant	1.8783	3.5	Not Significant
<u>Parameter Interactions</u>						
Flaw Size/ NDE Method	2.6317	1.6664	Significant	6.5768	1.6664	Highly Significant
Flaw Size/ Technician	2.8208	1.5173	Significant	0.97711	1.0	Not Significant
NDE Method/ Technician	1.3079	1.3940	Not Significant	2.1150	1.0	Significant
Flaw Size/ NDE Method/ Technician	0.57387	1.2214	Not Significant	0.78966	1.0	Not Significant

variance among individual technicians is believed to be the area of human factors, which should be further investigated.

- There were no significant differences (excepting the one depot previously mentioned) found between individual installations, between individual Commands, or between field installations and depots. Neither was there a significant difference observed between technicians using different manufacturer's equipment. The primary source of variance between the individual technicians was in the human factors area, which remains to be further investigated. The specific variables of formal education, age, classification skill level, NDE experience, and NDE training were each analyzed and proved to have only minimal influence on resulting NDE performance.
- Another human factors aspect which has been observed but has not been treated in this program is the false call level. There is not yet a generally agreed upon method for analyzing the impact of these false calls on NDE reliability. The raw data show extremely high false counts for some technicians performing radiographic NDE, for example. The success scores for these individuals are therefore suspect because a number of finds can be attributed to pure chance. Fortunately, the instances of extremely high false call levels were not numerous enough to make the total data picture suspect.
- The state of the art is constantly changing in NDE technology. Inspection reliability improvements resulting from recent refinements in NDE methods, presently undergoing service evaluation were not included in this study. There was a limited opportunity to evaluate the effectiveness of semi-automatic eddy current and ultrasonic equipment, beginning at the thirteenth base visit. Unfortunately, due to the late incorporation of semi-automated equipment into the program, the data quantity restricted determination of a reasonable confidence level for this equipment, but the mean crack detection levels obtained from using this type of equipment indicate that 90 to 95% reliability criteria may be possible at crack sizes considerably smaller than 1/2 inch.

Some recommendations derived from this study are considered generally significant and repeated below:

- Near-Term NDE Reliability Improvements

The primary source of the variance in the data collected and the main cause of failure to detect flaws is attributed to the human factor element in the NDE process. There is a need to concentrate on a practical method to evaluate the proficiency of the NDE technician as well as an increased evaluation, skill development and motivation of the NDE technician. Practical NDE examinations should be periodically administered to technicians and their performance ratings made available to them. Opportunities for periodic re-examination and flaw detection practice on hardware would be highly desirable. Mandatory review of ratings by supervision and management would provide avenues for positive motivation.

- Proficiency Determinations

The commonly assumed indicators of NDE proficiency such as technician skill level, years of experience, maturity in terms of age, extent of formal education, and hours of formal NDE training do not provide a true indication of flaw detection proficiency. True proficiency of a technician must be evaluated by methods which have a proven and consistent relationship with actual flaw detection capabilities. Practical examinations, administered with actual flaws in hardware, should be developed for such proficiency determinations. Standards of performance need to be established with the norms set to attainable goals defined from experience.

- Equipment

The state-of-the-art equipment used on both the pre-data acquisition trials and in the data acquisition itself, was inherently capable of response to the majority of flaws in the structure samples. In some cases with ultrasonic NDE, it was not possible to routinely obtain responses from all the flaws. The question then remains: why is a flaw missed if the process is capable of detection? One important

answer is: the equipment and/or process does not sufficiently alert the operator when a flaw response is present. There are potential solutions to this problem within the realm of equipment design improvements. Several of these are to: (a) improve the signal-to-noise ratio in both detection and readout functions, (b) promote operator vigilance through enhanced stimuli to sensual perception, (c) channel the operator's attention to flaw indications, (d) provide positive assurances that the equipment is performing its intended functions, and (e) automatically program and control those functions which are susceptible to human error.

Each of these potential equipment improvements is within the scope of today's technology. Programs to develop improved versions of NDE equipment and processes should include guidance from the technician/user, including engineering specialists and the personnel responsible for the day-to-day NDE operations. Full advantage should be taken from the advent of microprocessors and newly emerging electronic devices which provide a number of powerful, complex operations for signal processing, instrument readout and process control. In all cases, any "improved" version of equipment should demonstrate its flaw detection reliability under realistic conditions. Improved resolution, sensitivity and flaw characterization features do not automatically imply superior flaw detection reliability. The process of detection is uniquely different from the interrogation of the flaw itself. In some cases, the function of flaw searching should be separated from the activity of flaw interrogation. Therefore, different equipment designs or approaches may be necessary to optimize each.

13.6.3 Operationally Induced Defects on Surface Opposite Nondestructive Examination

Data pertinent to piping for cracks initiated by mechanisms such as IGSCC or thermal fatigue are quite limited, at least in the open literature. Only two studies are available, although others are known to exist.

Reinhart^(13.6.18) reported on a round-robin UT to detect IGSCC and lack-of-fusion in austenitic steel piping. Table 13.6.11 reproduced from reference 13.6.18 permits a qualitative assessment of the ability to detect IGSCC. Hopefully, more data on detection of IGSCC will become available.

The results of inservice NDE-UT on an operating reactor were reported for cracked safe ends at Duane Arnold.^(13.6.19) All of the eight nozzles contained cracks with one being through-wall. The other seven contained cracks in excess of 50% through-wall. Two aspects are of concern with regard to the Duane Arnold nozzles. The first is the initiation of IGSCC in a crevice, its propagation around the inner surface, then its relatively uniform propagation rate through the wall. Such a crack is of concern because the uniform loss of ligament without going through the wall could lead to a guillotine break under a shock load. The second aspect concerns the low UT-DAC levels with cracks over 50% through the wall.

Becker^(13.6.20) reported on the detection of both electro-discharge machining notches and fatigue cracks, primarily for ferritic steels in wall thicknesses comparable to nuclear piping. The results tend to parallel those at Duane Arnold^(13.6.19) in there being signals; however, the signals were below the reporting levels (Figure 13.6.4).

13.6.4 Current Status Experimental Reliability Data

The ultimate aim in the NDE reliability analyses is to quantify the probability of detection of each class of flaws as functions of the various internal and external factors discussed in Section 13.7. Ideally, one hopes to control both internal and external factors sufficiently that a well-defined probability of detection function with minimal variance is obtained. Unfortunately, this is rarely the case, particularly in the examination of weldments in piping. We lack adequate experimental data, do not know the effects of interacting internal and external parameters, nor the variability due to operators.

TABLE 13.6.11. Summary of IGSCC Test Results by Quadrant

SAMPLE IDENTIFICATION	PIPE DIAMETER (in)	PIPE SECTION (deg)	BASIS FOR EVALUATION (QUADRANTS)	TEST TEAM RESULTS ^{b, c}																				
				A				B				C				D				E				
				1	2	3	4	1	2	3	4	1	2	3	4	1	2	3	4	1	2	3	4	
19AL	10	180																						
1028A		36																						
1028																								
1024A																								
B2A		4	360																					
10K18	10	360																						
1021																								
10K17L																								
10K17																								
1020A																								
1024B		180																						
1028C		36																						
1028D																								
1019A		360																						
198L		180																						
A9A	4	360																						

^a LOCATION DETERMINED BY DYE PENETRANT TEST: UNFLAWED QUADRANT FLAWED QUADRANT

^b TEST PROCEDURE, PHASE A (INSPECTION GROUP) PHASE B (EPRI)

^c RESULTS AS SHOWN: CRACK CALLED CRACK SIGNAL PLOTTED, NOT CALLED CRACK NO CRACK DETECTED

FIGURE 13.6.4. Predicted Probability of Detection (POD) Curves for 20, 50, and 100% DAC Reporting Levels

FIGURE 13.6.4. Predicted Probability of Detection (POD) Curves for 50%, 90%, and 100% DAC Reporting Levels

Flaw detection reliability with ultrasonics has been discussed in Chapters 4 and 5. Figure 13.7.1 presents those internal and external factors significant to such reliability. This figure is somewhat modified and expanded from Figure 4.6.1.

The external and internal factors cited vary in relative significance; however, any one of them can reduce reliability to zero under a given set of circumstances. This is particularly true for flaw characteristics. Several of

FIGURE 13.7.1. Limitations to Flaw Detection, Sizing, Location (modified from Figure 4.6.1)

the factors are interactive. For example, varying the equipment may substantially improve or reduce the reliability as influenced by flaw characteristics.

In the case of RT, the tightness and orientation of two-dimensional cracks and the fraction of the section thickness covered by the crack will be the critical factors.

Eddy current testing will be affected by many of the factors in Figure 13.7.1, both internal and external.

In the case of MT, PT, VT the surface characteristics will be controlling. A rough eroded surface will be much more difficult than a smooth surface insofar as detecting surface flaws is concerned.

Thomas^(13.7.1) argues that NDE, particularly ISI, is overrated. He tends to concentrate on ISI of the RPV rather than other components such as piping in the primary circuit. Two approaches to ISI are cited; namely, problem monitoring and problem seeking. Problem monitoring consists of resolving a course of action for a known problem. Problem seeking, on the other hand, attempts to seek out unknown or unexpected modes of failure. The author considers this approach relatively ineffective.

Thomas^(13.7.1) argues that ISI in general provides only a limited factor of improvement; specifically, no more than a factor of 3. In my estimation, this number is suspect because the cases of both vessel and piping failures used to validate this factor of 3 were either non-nuclear, without ISI, or the piping data included instrument line failures which biases heavily toward leaks and failures not detected by ISI. A further factor subject to argument has to do with the credit taken for detection of flaws followed by repair.

Another approach, which appears to have considerable potential, is suggested by Buchalet et al.^(13.7.2) In essence, they suggest that the level of NDE be based on a qualitative sensitivity analysis using the following three parameters:

1. the "vulnerability" of a given zone of the primary coolant system, with the level depending upon the potential damage to which the zone could be subjected during the life of the plant.

2. the "importance of the safety hazard" created by a rupture in a given zone of the primary coolant system.
3. the "efficiency level" of current ISI programs for a given zone of the primary coolant system.

Vulnerability is determined in terms of the following factors:

1. mechanical and thermal loads caused by geometrical discontinuities, external loads, metallurgical discontinuities, thermal gradients, and residual stresses.
2. fabrication and examination factors such as volumetric defects, planar defects, metallurgical defects and difficulty of examinations during fabrication.
3. environmental factors such as corrosion due to flow rates, to liquid-vapor interfaces, to local boiling, or to possible leaks, erosion and wear, mating, tearing, impact shock or irradiation.

The preceding factors are given weighting functions and a 1, 2, 3 rating for each region of the primary coolant system where

- 1 = low probability of having the damage factor present in the zone considered
- 2 = low to medium probability
- 3 = high probability.

The significance of the safety hazard due to failure in a given zone can be assessed as low, medium, or high. Based on the preceding, five categories of ISI are established within the primary coolant system zone; namely,

- I - No inservice inspection
- II - Low efficiency ISI
- III - Medium efficiency--the rating for current ISI
- IV - High efficiency, ISI
- V - Design changes are required to decrease the vulnerability level.

Figure 13.7.2^(13.7.2) illustrates the use of these zones (I, II, III, IV, V) for a specific case of flaws in reactor pressure vessel nozzles.

FIGURE 13.7.2. Global Evaluation of the Primary Coolant System Zones

13.7.1 Statistical or Probabilistic Approaches

Another method of defining NDE reliability is in terms of sensitivity where testing sensitivity is determined by the minimum size of a flaw which must be detected based on engineering requirements. If the sensitivity is too low, flaws may be missed that could result in failure. Too much sensitivity caused detection of large numbers of structural inhomogeneities and insignificant flaws.

Rogovsky and Rose^(13.7.3) discuss a statistical/probabilistic approach to optimization of NDE sensitivity using the concept of probability density functions in Figure 13.7.3. While the authors base their approach on distance-gain-size (DGS), other approaches based on standards or standardless (e.g., DGS) could be used. They define the signal amplitude A as a function of a random argument where defect characteristics, S , l parameters pertaining to the piece being formed and m apparatus sensitive parameters so

$$A = y(S, X, \dots, X_i, \dots, X_{l+m})$$

In terms of signal amplitudes, $P_1(A')$ corresponds to flaw sizes less than critical and $P_2(A'')$ corresponds to flaw sizes greater than critical (Figure 13.7.3). The probability of a false rejection γ and of passing a flaw β can be expressed as

$$\beta = \int_{A''_{\min}}^{A_0} P_2(A'') dA$$

and,

$$\gamma = \int_{A_0}^{A'_{\max}} P_1(A') dA$$

FIGURE 13.7.3. Sample Distributions of the Probability Density Function $p(A)$ of the Signal Amplitudes in the Reject-Free ($S < S_0$) and the Rejectable ($S \geq S_0$) Areas of a Part

Using the above approach, the following is a general formula for the probability density of the signal amplitude:

$$P(A_i) = \int_{A_s} \int_{y_i} \dots \int_{y_1} \dots \int_{y_m} \int_S \delta(A - A_j) dA P_{AS}(A_s) dA_s \times \prod_{i=1}^m P_y(y_i) dX_i$$

The authors utilize measurements with FBHs to calibrate their model.

GENERATION OF PROBABILITY DENSITY FUNCTIONS AND
CUMULATIVE DENSITY FUNCTIONS

The ultimate aim is the generation of probability density functions if one is to quantify detection reliability as an input into a probabilistic fracture mechanics model. This aspect is dealt with fairly comprehensively in Chapter 12. The status at this time, with regard to piping or thicknesses relevant to piping, is that there are inadequate quantitative data available to construct appropriate PDFs and CDF's.

If one examines the available data in terms of the various UT evaluation procedures, it appears that distance-amplitude-correction (DAC) has inherent limitations, particularly with regard to flaws oriented away from the normal. Other procedures such as adaptive learning may lead to improved reliability with regard to both detection, location and sizing. Further work is required to quantify these advanced techniques.

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CHAPTER 13A

PROBABILITY MODELS FOR RELIABILITY OF FLAW DETECTION

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CHAPTER 13A

PROBABILITY MODELS FOR RELIABILITY OF FLAW DETECTION

13A.1

INTRODUCTION

This chapter will examine general and specific probability models, then pursue these models through correlation functions and field or production data. Finally, statistical probability models for flaw detection will be investigated. Because of the spectrum of terminology, a section on nomenclature is included. The equations in Sections 13A.3 and 13A.4 are derived predominantly from References 13A.3.1 through 13A.3.5.

The mathematical derivations are considered rigorous; however, it should be recognized that the equations assume correlations between four parameters such as size and ultrasonic signals. Such correlations usually will be non-linear, may be discontinuous, and almost certainly are influenced by surface and internal properties of the test piece. Therefore, one should examine so-called generic correlations carefully since they may be neither generic nor correlate over a spectrum of flaw sizes, shapes, locations and orientations.

NOMENCLATURE

a	Flaw surface depth or internal half depth
a	Apparent flaw depth
a_r	Apparent flaw rejection threshold
b	Minimum flaw size of concern
$2c$	Flaw length
$2c$	Apparent flaw length
$2c_r$	Apparent flaw length rejection threshold
f	Fraction of population that contains a flaw of the size and type under consideration
f_f	Fraction of units that fail
f_r	Fraction of units that are rejected by inspection
f_D	Fraction of defective units in service
n	Number of subvolumes with flaws
$pd(a_r)$	Probability distribution of rejection levels
$pd(a/i)$	Probability distribution of flaw size given a response signal of magnitude i
$pd(a/s)$	Probability distribution of flaw size given a detection response signal of amplitude s
$pd(a/D,a)$	Probability distribution of flaw size given the detection event and the flaw has an apparent size a
$pd(a/D,a)$	Probability distribution of apparent flaw size given the detection event and the actual flaw size is a
$pd(i/a)$	Probability distribution of flaw response signal magnitude given a flaw size of a
$pd(s/a)$	Probability distribution of detection signal amplitude given a flaw of size a
$pn(a)$	Preinspection distribution of flaw sizes

$pn(a,S)/r$	Post-inspection distribution of flaw size
$pn(a,S,r)$	Distribution of flaw sizes that cause rejection
$pn(D,a)$	Distribution of detected apparent flaw sizes
$pn(i)$	Distribution of flaw response signal magnitude
$pn(s)$	Distribution of flaw detection response amplitude
s	Flaw response amplitude
s_D	Flaw response amplitude detection threshold
v	Number of subvolumes
A	Area which may contain flaws, m^2
IRP	$P(r/a)$ flaw rejection probability
L	Length which may contain flaws, m
N	Number of tests
P	True probability of detection
$\hat{P}$	Natural estimate of $P(S_n/n)$
P'	Lower bound for P (probability of detection at a given confidence level)
P_0	Probability of no flaws in given subvolume
P_1	Probability when flaw site is known
P_2	Probability when flaw site is unknown; many flaws; total inspection
P_3	Probability when flaw site is unknown; many flaws; partial inspection
P_4	Probability when flaw site is unknown; many flaws; partial inspection; flaw rate in uninspected region differs from rate in inspected region
P_5	Probability when flaw site is unknown; many flaws; partial inspection; flaw rate varies among various regions
$P(D)$	Probability of a flaw indication (of certain size)

$P(E)$	Probability of no flaw indication = $1 - P(D)$
$P(F)$	Probability that a flaw of a certain size exists
$P(G)$	Probability that a flaw of a certain size does not exist = $1 - P(F)$
$P(D/F)$	Probability of detecting a flaw when one exists
$P(E/F)$	Probability of missing a flaw = $1 - P(D/F)$
$P(D/G)$	Probability that a false indication will occur
$P(E/G)$	Probability of verifying a good part is flaw-free = $1 - P(D/G)$
$P(G/E)$	Probability a flaw does not exist when none is indicated [or, is likelihood of correct acceptance (reliability)]
$P(F/E)$	Probability that a flaw exists even though none is indicated [or, is likelihood of false acceptance (a type-one inspection error)]
$P(G/D)$	Probability that a flaw does not exist even though a flaw has been indicated [or, is likelihood of false rejection (a type-two inspection error)]
$P(F/D)$	Probability that a flaw exists when a flaw is indicated [or, is likelihood of correct rejection]
$P_D = P(E/F)$	Probability that a material unit that passes the inspection is defective; also $1 - P(r/a, S)$
$P_{D0} = P(F)$	Probability that a material is defective before inspection
$P(D/a)$	Flaw detection probability
$P(f)$	Probability of failure event ($P(f/a)$)
$P(f/a)$	Flaw failure probability
$P(n)$	Probability as defined in specific equations, n flaws
$P(r)$	Probability of the rejection event
$P(r/(a, S))$	Flaw rejection probability; defined as the probability that a material (unit) will be rejected from the population by an inspection, given that the material unit contains only one flaw of size "a" and that the inspection size is S
$P(r/D, a)$	Conditional flaw rejection probability

$P_{SR} = P(E/G)$	Probability that a sound material unit will be rejected by inspection
PN_D	Probable number of defects per material unit in population that passes inspection
PN_{D0}	Probable number of defects per material unit before inspection
$PN(f)$	Probable number of failures per unit
PN_r	Probable number of rejectable indications per material unit
PN_{Sr}	Probable number of rejectable indications that are due to flaws of size less than b
R_1, R_2, R_3, R_4, R_5	Reliabilities; subscripts denote cases similar to those for subscripted probabilities
S	Inspection size; defined as the flaw size at which the flaw rejection probability is 0.50
S_N	Number of detections of flaws in the tests
V	Volume which may contain flaws, m^3
V_u	Total volume of component
Z	Standardized binomial (normal) variable
$Z_{\alpha'}$	Tabulated standard normal variable for $1 - \alpha'$
$1 - \alpha'$	Confidence probability
α	Quality index = $\frac{P(G)}{P(F)} = \frac{1-P(F)}{P(F)}$
$\alpha \approx$	Ratio of probability that a flaw does not exist to the probability that a flaw does exist
$\alpha \approx$	$1/f$
β	Error probability ratio = $\frac{P(D/G)}{P(D/F)}$
β	Ratio of the probability of a false indication to the demonstrated probability of flaw detection
γ	Success-probability ratio = $\frac{P(E/G)}{P(D/F)} = \frac{1-P(D/G)}{1-P(D/F)}$

γ	Ratio of probability of correct verification of flaw-free parts to the probability of missing a flaw
δ	Measure of inspection uncertainty
λ	Mean number of flaws per unit length, area, or volume; m^{-1} , m^{-2} , m^{-3}
ρ	Proportion of length, area, or volume of piece inspected
σ	Number of subvolumes in V

SUBSCRIPTS

I	Inspection
M	Manufacturing
T	Total
x	Generalized terminology for given examination method (PT, MT, RT, UT, ET, VT, etc.) (P_{MT} , δ_x , etc.)
y	Generalized material terminology (Al, Steel, etc.)
erf z	Error function
erfc z	Complementary error function

Reliability is defined as $P(G/E)$ - the probability that a flaw does not exist when none is indicated.

The conditional probability of Event A given Event B, denoted by $P(A/B)$ is defined as the ratio of the probability of the intersection^(a) of A and B to the probability of event B; i.e.,

$$P(A/B) = P(AB)/P(B) \quad (13A.3.1)$$

Correspondingly,

$$P(B/A) = P(BA)/P(A) \quad (13A.3.2)$$

Since the probability of the intersection of A and B is equal to the probability of the intersection of B and A, one can combine Equations (13A.3.1) and (13A.3.2) above and write

$$P(A/B)P(B) = P(AB) = P(BA) = P(B/A) P(A) \quad (13A.3.3)$$

This becomes Bayes theorem:

$$P(A/B) = \left[\frac{P(B/A)}{P(B)} \right] P(A) \quad (13A.3.4)$$

Several cases will be examined using the specific terminology for flaw probabilities based on References 13A.3.1 and 13A.3.2.

(a) Intersection of A and B is the set consisting of all the outcomes that are both in A and B. It may be written $P(A \cap B)$ or $P(AB)$.

13A.3.1 Simple Case: Flaw Site Known (P_1, R_1)

$$P(GE) = P(G/E) P(E) \quad (13A.3.5)$$

$$\text{Reliability} = R_1 = P_1(GE)/P_1(E) \quad (13A.3.6)$$

is the probability that a flaw does not exist when none is indicated; however, another probability is $P_1(F/E)$, the probability that a flaw exists even though a flaw has not been indicated; e.g., E can occur if either G or F occurs:

$$P_1(E) = P_1(E/F) P_1(F) + P_1(E/G) P_1(G) \quad (13A.3.7)$$

By analogy with Equation (13A.3.5)

$$P_1(GE) = P_1(E/G) P_1(G) \quad (13A.3.8)$$

Substituting Equation (13A.3.7) and (13A.3.8) into (13A.3.6),

$$R_1 = P_1(G/E) = \frac{P_1(E/G) P_1(G)}{P_1(E/F) P_1(F) + P_1(E/G) P_1(G)} \quad (13A.3.9a)$$

Rearranging,

$$P_1(G/E) = \frac{1}{\frac{P_1(E/F) P_1(F)}{P_1(E/G) P_1(G)} + \frac{P_1(E/G) P_1(G)}{P_1(E/G) P_1(G)}} \quad (13A.3.9b)$$

$$R_1 = P_1(G/E) = \frac{1}{1 + \frac{P_1(E/F) P_1(F)}{P_1(E/G) P_1(G)}} \quad (13A.3.9c)$$

An alternate form of Equation (13A.3.9c) is based on the fact that the following are mutually exclusive events:

$$P_1(G) = 1 - P_1(F) \quad (13A.3.9d)$$

and

$$P_1(E/F) = 1 - P_1(D/F) \quad (13A.3.9e)$$

so Equation (13A.3.9c) can be expressed in terms of unreliability before inspection $P_1(F)$ and the probability of detecting a flaw $P_1(D/F)$

$$R_1 = \frac{1}{1 + \frac{[1 - P_1(D/F)] P_1(F)}{P_1(E/G)(1 - P_1(F))}} \quad (13A.3.9f)$$

where

$$\alpha = \text{Quality Index} = \frac{P_1(G)}{P_1(F)} = \frac{1 - P_1(F)}{P_1(F)} \quad (13A.3.10)$$

$$\gamma = \text{Success-probability ratio} = \frac{P_1(E/G)}{P_1(E/F)} = \frac{1 - P_1(D/G)}{1 - P_1(D/F)} \quad (13A.3.11)$$

Therefore, Equation (13A.3.9c) can be written as

$$R_1 = P_1(G/E) = \frac{1}{1 + \frac{1}{\alpha \gamma}} \quad (13A.3.12)$$

Similarly, the following general cases can be derived:

$P_1(F/E)$ = the probability that a flaw exists even though none is indicated:

$$P_1(F/E) = \frac{P_1(F) P_1(E/F)}{P_1(G) P_1(E/G) + P_1(F) P_1(E/F)} \quad (13A.3.13)$$

$$P_1(F/E) = \frac{1}{1 + \alpha\gamma} \quad (13A.3.14)$$

$P_1(G/D)$ = the probability that a flaw does not exist even though a flaw has been indicated:

$$P_1(G/D) = \frac{P_1(G) P_1(D/G)}{P_1(G) P_1(D/G) + P_1(F) P_1(D/F)} \quad (13A.3.15)$$

$$P_1(G/D) = \frac{1}{1 + \frac{1}{\alpha\beta}} \quad (13A.3.16)$$

where

$$\beta = \text{error} = \text{probability ratio} = \frac{P_1(D/G)}{P_1(D/F)} \quad (13A.3.17)$$

$P_1(F/D)$ = the probability that a flaw exists when a flaw is indicated:

$$P_1(F/D) = \frac{P_1(F) P_1(D/F)}{P_1(G) P_1(D/G) + P_1(F) P_1(D/F)} \quad (13A.3.18)$$

$$P_1(F/D) = \frac{1}{1 + \frac{1}{\alpha\beta}} \quad (13A.3.19)$$

13A.3.2 Flaw Sites Unknown--100% Inspected (R_2, P_2)

Generally, in an object such as a pressure vessel, a flaw may be considered a "rare event" in the sense that most of the part (length, area, volume) is unflawed. The first step is to establish the probability distribution for the total number of flaws in the system.

The number of flaws within a volume, V , will be assumed to be a random number; consequently, a volume under inspection may have 0, 1, 2--or any number n flaws.

Suppose the volume, V , is subdivided into equal small volumes σ , and that the probability that an unacceptably large flaw is present in a specified sub-volume is proportional to the volume of the subvolume. Then if λ is a constant of proportionality,

$$P_1 = \lambda \frac{(V)}{\sigma} \quad (13A.3.20)$$

The probability of no flaws in (V/σ) is

$$P_0 = 1 - \lambda \frac{V}{\sigma} - \left(\frac{\lambda V}{\sigma}\right)^2 \quad (13A.3.21)$$

where the third term is the probability that two or more flaws exist in the subvolume; this term is of higher order than the second; if σ is chosen large enough, the third term becomes negligible. The probability of finding n subvolumes with flaws is a series of Bernoulli trials where each subvolume V/σ is a trial with a probability of success, P_1 , and failure, P_0 :

$$\begin{aligned} p(n; \sigma) &= \frac{\sigma!}{(\sigma-n)! n!} (P_1)^n (P_0)^{\sigma-n} \\ &= \frac{\sigma!}{(\sigma-n)! n!} \left(\frac{\lambda V}{\sigma}\right)^n \left(1 - \frac{\lambda V}{\sigma}\right)^{\sigma-n} \end{aligned} \quad (13A.3.22)$$

However, the subdivision into subvolumes was arbitrary, and in no way can an arbitrary subdivision alter the mean number of unacceptable flaws, (λV) , contained in V . Therefore,

$$\begin{aligned} P(n) &= \sum_{\substack{\sigma \rightarrow \infty \\ \lambda V = \text{constant}}} \frac{P(n; \sigma) = (\lambda V)^n \exp^{-\lambda V}}{n!} \end{aligned} \quad (13A.3.23)$$

Equation (13A.3.23) gives the probability that exactly n flaws are present in the volume, V . From the derivation, λ can be seen to be the average number of flaws per unit volume; whereas λ and λV need not be integers, n obviously must be an integer. Equation (13A.3.23) is the Poisson distribution and is tabulated in most elementary statistics tests.

The probability that the system contains n flaws is $p(n)$. For line welds, λ is the mean number of flaws per unit length and L is the length. For surfaces and thin sheet, λ is the mean number of flaws per unit area and A is the area; for large solids, λ is the mean number of flaws per unit volume and V is the volume of the vessel wall, etc. Whereas n is, of course, an integer, λ may be a decimal. The product λV , λL , or λA is the mean number of flaws per system; the product need not be an integer.

13A.3.2.1 Flaw Distribution Within a Volume

When several flaws are distributed within a volume, all flaws need to be detected to determine whether they are unacceptable because of limited size, or because they require evaluation or repair. In contrast to small parts, a vessel is not rejected if a flaw is found. If the detection of any flaw is assumed to be independent of any other flaw,

$$P [FE] = \sum_{n=1}^{\infty} P(F_n)P(E/F_n) \quad (13A.3.24)$$

where $P(F_n)$ is the probability that exactly n flaws are present in V and $P(E/F_n)$ is the probability that all of them escape detection. To get the total probability that a flawed part misses inspection, one must sum over all possible number of flaws n .

By applying the previous criteria,

$$P(F_n) = p(n) = \frac{(\lambda V)^n}{n!} \exp(-\lambda V) \quad (13A.3.25)$$

Also, under the assumptions of flaw independence and detection independence, the roles of conditional probability hold, and

$$P(E/F_n) = [P(E/F_1)]^n = [P(E/F)]^n \quad (13A.3.26)$$

Therefore, Equation (13A.3.24) becomes

$$P(FE) = \exp(-\lambda V) \sum_{n=1}^{\infty} \frac{(\lambda V)^n}{n!} [P(E/F)]^n \quad (13A.3.27)$$

The reliability for the case where flaw sites are unknown; many flaws; total inspection (R_2, P_2) is

$$R_2 = P_2(G/E) \quad (13A.3.28a)$$

The unreliability is

$$1 - R_2 = P_2(F/E) \quad (13A.3.28b)$$

Since

$$P_2(FE) = P_2(F/E)P_2(E) = P_2(E/F)P_2(F) \quad (13A.3.29)$$

$$1 - R_2 = P_2(F/E) = \frac{P_2(FE)}{P_2(E)} \quad (13A.3.28c)$$

$$P_2(E) = P_2(FE) + P_2(GE) \quad (13A.3.30a)$$

$$= P_2(FE) + P_2(E/G) P_2(G) \quad (13A.3.30b)$$

or

$$R_2 = 1 - \frac{1}{1 + \frac{P_2(E/G) P_2(G)}{P_2(FE)}} \quad (13A.3.31)$$

From $P_2(G)$ (no flaw exists)

$$P_2(G) = P(o) = (\lambda V)^0 \exp(-\lambda V) = \exp(-\lambda V) \quad (13A.3.32)$$

Since

$$\sum_{n=1}^{\infty} \frac{x^n}{n!} = e^{x-1}; \quad \sum_{n=1}^{\infty} \frac{(\lambda V)^n (P(E/F))^n}{n!} = \exp[(\lambda V)P(E/F)] - 1 \quad (13A.3.33)$$

$$R_2 = 1 - \frac{1}{1 + \frac{P_2(E/G) \exp(-\lambda V)}{\exp(-\lambda V) \exp[(\lambda V)P_2(E/F)] - 1}} \quad (13A.3.34a)$$

$$R_2 = \frac{1}{1 + \frac{\exp[(\lambda V)P_2(E/F)] - 1}{P_2(E/G)}} \quad (13A.3.34b)$$

Equation (13A.3.34b) is analogous to Equation (13A.3.9f).

13A.3.3 Flaw Site Unknown -- Only Part of the Line, Area, or Volume Inspectable (R_3, P_3) and (R_4, P_4)

In many situations, portions of a structure are inaccessible for inspection as is the case for portions of a reactor pressure vessel, particularly in older plants. Such portions may contain flaws. Because uninspectable volumes may be conservatively constructed to compensate for uninspectability, the expected number of critical flaws per unit volume within them may differ from the number within inspectable volumes. In this section, the equation for

reliability when portions of the structure are uninspectable will be derived. The general case is where $\lambda_a \neq \lambda_\sigma$; that is, when the flaw rate of occurrence in the inspectable region differs from the rate in the uninspectable region.

The desired equation may be derived from Equations (13A.3.23) and (13A.3.34b). The probability that no flaws exist in the uninspected region is

$$P(o) = \exp [-(1 - \rho)\lambda_a V] \quad (13A.3.35)$$

where $1 - \rho$ is the proportion of V which is not inspected. The probability that no flaw exists in the inspected region after inspection is

$$R_2 = \frac{1}{1 + \frac{\exp [\rho\lambda_\sigma VP_2(E/F)] - 1}{P_2(E/G)}} \quad (13A.3.36)$$

as long as the probability of occurrence of flaws in one region is independent of that in the other, the law of compound probability applies so

$$R_4 = P(o)R_2 = \frac{\exp [-(1-\rho)\lambda_a V]}{1 + \frac{\exp [\rho\lambda_\sigma VP_4(E/F)] - 1}{P_4(E/G)}} \quad (13A.3.37)$$

13A.3.3.1 Case Where $\lambda_a = \lambda_\sigma$

If the flaw density is the same in the uninspected region as in the inspected region, $\lambda_a = \lambda_\sigma = \lambda$, then

$$R_3 = \frac{\exp [-(1-\rho)\lambda V]}{1 + \frac{\exp [\rho\lambda VP_3(E/F)] - 1}{P_3(E/G)}} \quad (13A.3.38)$$

13A.3.3.2 Many Sections

If a structure contains many sections with various flaw densities, the rules of compound probability can be applied. For m uninspectable regions and ℓ inspectable regions the general form is

$$R_5 = \frac{\exp\left(-\sum_{k=1}^m \lambda_k V_k\right)}{\prod_{j=1}^{\ell} \left[1 + \frac{\exp\left[\lambda_j V_j P_j (E/F)\right] - 1}{P_j [E/G]} \right]} \quad (13A.3.39)$$

In considering the explicit application of probability theory to flaw detection we return to Bayes Theorem:

$$\begin{aligned} P(A/B) P(B) &= P(B/A) P(A) \\ P(F/D) P(D) &= P(D/F) P(F) \end{aligned} \quad (13A.3.4)$$

(terminology used)

$$pd(a/i) da = P(F/D) \quad (13A.3.40)$$

$$pd(i/a) di = P(D/F) \quad (13A.3.41)$$

$$\frac{pn(a) da}{\int_0^{\infty} pn(a) da} = P(F) \quad (13A.3.42)$$

and

$$\frac{pn(i) di}{\int_0^{\infty} pn(i) di} = P(F) \quad (13A.3.43)$$

By combining the four Equations (13A.3.40 to 13A.3.43) into Bayes Theorem

$$pd(a/i)pn(i) \int_0^{\infty} pn(a)da = pd(i/a)pn(a) \int_0^{\infty} pn(i)di \quad (13A.3.44)$$

If we assume that each indication corresponds to a flaw of some size (possibly zero) and each flaw corresponds to an indication of some size (possibly zero), then

$$\int_0^{\infty} pn(a)da = \int_0^{\infty} pn(i)di \quad (13A.3.45)$$

and Equation (13A.3.44) can be reduced to

$$pd(a/i)pn(i) = pd(i/a)pn(a) \quad (13A.3.46)$$

Furthermore,

$$\int_0^{\infty} pd(a/i)da = \int_0^{\infty} pd(i/a)di = 1 \quad (13A.3.47)$$

Integrating both sides of Equation (13A.3.46) over i from zero to infinity gives

$$\int_0^{\infty} pd(a/i)pn(i)di = pn(a) \int_0^{\infty} pd(i/a)di \quad (13A.3.48)$$

From Equation (13A.3.47), the integral on the right-hand side of Equation (13A.3.48) is one; hence,

$$pn(a) = \int_0^{\infty} pd(a/i)pn(i)di \quad (13A.3.49)$$

Substituting Equation (13A.3.49) into Equation (13A.3.46) and rearranging terms gives

$$pd(i/a) = pd(a/i)pn(i) \left[\int_0^{\infty} pd(a/i)pn(i)di \right]^{-1} \quad (13A.3.50)$$

The problem becomes one of establishing a relationship between the signal magnitude (i) and the detection signal amplitude (s) as they relate to the flaw size (a). The preceding relationships in Equations (13A.3.40 to 13A.3.50) develop a relationship between (i) and (a). The equations to be developed later will be in terms of (s), the amplitude. While simplifying assumptions can be made in terms of linear relationships, the more correct magnitude-amplitude curves will be nonlinear.

Equations (13A.3.1 to 13A.3.39) concentrated on the various probabilistic relationships in very general terms. Equations (13A.3.40 to 13A.3.50) established a relationship between NDE signal and flaw size in a qualitative sense. The next step is to attempt to quantify the inspection and flaw variables.

We shall attempt to relate the imperfection rejection probability $P(r/(a,S))$ to inspection acceptance level in terms of apparent flaw sizes and the flaw sizes, (a). For example, a typical inspection size is the flaw size that has a 50% probability of causing a rejection if it exists. Obviously, such a level can be raised or lowered if deemed necessary.

The post-inspection flaw distribution $pn(a,S)/r$ is related to the initial flaw distribution function $pn(a)$ by

$$pn((a,S)/\bar{r}) = (1 - P(r/(a,S))) (pn(a)) \quad (13A.3.51)$$

The distribution of flaw sizes that cause rejection $pn(a,S,r)$ is related to the imperfection rejection probability and to the initial flaw distribution function $pn(a)$ by

$$pn(a,S,r) = P(r/(a,S)) (pn(a)) \quad (13A.3.52)$$

These may be modified so that

$$\frac{pn((a,S)/\bar{r})}{pn(a)} = 1 - P(r/(a,S)) \quad (13A.3.51a)$$

$$P(r/(a,S)) = 1 - \frac{pn((a,S)/\bar{r})}{pn(a)} \quad (13A.3.51b)$$

$$P(r/(a,S)) (pn(a)) = pn(a) - pn((a,S)/\bar{r}) \quad (13A.3.51c)$$

but

$$P(r/(a,S)) (pn(a)) = pn(a,S,r) \quad (13A.3.52)$$

Therefore,

$$pn(a,S,r) = pn(a) - pn((a,S)/\bar{r}) \quad (13A.3.52a)$$

A third relationship of $P(r/(a,S))$ is

$$P(r/(a,S)) = P(r/(D,a,S)) P(D/a) \quad (13A.3.53)$$

where the conditional rejection probability $P(r/(D,a,S))$ is the probability the component will be "rejected," given the detection event has occurred, the flaw of size (a) exists, and the detection probability $P(D/a)$ is the probability that a specified detection event occurs; given that a flaw of size (a) exists in the component. The determination of the detection probability and the conditional rejection probability involves the establishment of the underlying inspection frequency functions, the inspection amplitude frequency function $pd(S/a)$ for $P(D/a)$, and the apparent size frequency function $pd(a/(D,a))$ for $P(r/(D,a,S))$. If these inspection frequency functions are known, the distributions of amplitudes observed in the product inspection and the distribution of

apparent flaw sizes can be used separately to arrive at independent estimates of the material quality, both at the time of preinspection and at various times throughout the system life.

A major problem is the lack of quantitative information regarding values of $pn(a)$. A typical flaw size distribution is given in Figure 13A.3.1, together with the ideal flaw detection function. The inspection capability as

FIGURE 13A.3.1. a) Typical Flaw Size Frequency Distribution $\phi(a)$; Maximum Flaw Size a_u and Maximum Allowable Flaw Size a_m ; b) Ideal Flaw Detection Function $\phi'(a)$ Required for Quantitative Failure Prediction Typical of Bounded Extremal (Weibull) Distribution

measured by $P(r/(a,S))$ may take the form of Figure 13A.3.2. The two figures are combined in Figure 13A.3.3 to illustrate a possible flaw detection function. Figure 13A.3.4 illustrates the impact of inspection uncertainty, δ , on flaw rejection. Figure 13A.3.5 is an example of what is needed from quantitative NDE such as UT, showing a typical confidence band and detectability limit a_0 . If detected flaws are analyzed or repaired, the corrected flaw distribution will be such as given in Figure 13A.3.6. Figure 13A.3.7 typifies the variability of reliability to inspection as a function of reliability before inspection.

FIGURE 13A.3.2. Hypothetical Curve of Probability of Rejection Versus Imperfection Size. "False" signals and 95% confidence levels shown.

FIGURE 13A.3.3. Flaw Detection Function Derived from Previous Figures

FIGURE 13A.3.4. Probable Fraction of Material Units Rejected

FIGURE 13A.3.5. Example of Flaw Size Measurement Required for Quantitative Ultrasonics Showing Typical Confidence Band and Detectability Limit a_0 .

FIGURE 13A.3.6. The Distribution of Imperfection After Inspection

FIGURE 13A.3.7. Variation of Reliability After Inspection as a Function of Reliability Before Inspection. The probability of identifying an existing flaw is a parameter. The false alarm rate, $P[B/A'] = 0.02$.

The following equation permits such an evaluation of $pn(a)$ when the rejection probability $P(r/a, S)$ is known and assuming no spatial correlation of flaws:

$$pn(a) = \frac{f_{ro}(S_o)pn_o((a, S_o)/r)}{P_o(r/(a, S_o))} \quad (13A.3.54)$$

where the inspection method is designated by (o) , the inspection size by S_o , and the flaw size distribution by $pn_o((a, S_o)/r)$. This represents the number of flaws per unit volume in a given size interval. Figure 13A.3.8 illustrates

FIGURE 13A.3.8. Relationship Between Number of Rejectable Indications and $p_{n_0}((a, S_0)/R)$

how a point estimate of $p_{n_0}((a, S_0)/r)$ can be determined from an examination of flaw sizes in rejected units $f_{r_0}(S_0)$ and how $P_0(r/(a, S_0))$ is determined by an analysis of the inspection data on specimens containing known flaws.

The historic pattern of fabrication and NDE for components such as pressure vessels has substantial built-in conservatism. This is recognized; however, it is difficult to establish how much conservatism is justified and how much is excessive. If it becomes possible to quantify NDE signals with flaw dimensions, orientation and location, and relate these to stresses and mechanical properties through a probabilistic failure model, the required level of conservatism could be established.

Three obvious areas of conservatism are:

1. The assumption a large flaw still exists in the component after inspection--in effect, although a flaw is not found, it must exist.

2. Extreme values of stress and mechanical properties are assumed to exist in conjunction with the assumed flaw.
3. Generally, the crack is assumed to propagate with zero initiation time, whereas crack initiation typically occurs only after a substantial portion of the system life.

These factors will be considered in terms of probability of flaw detection followed by assessment of flaw significance and repair if required; probability that significant flaws will not be detected; and the probability that undetected flaws could lead to failure. The reader may wish to read Chapter 11, Section 11.4, at this time to familiarize himself with hazards analysis and the form of such functions.

The probability of rejection by an inspection $P_r(S)$ on the fraction rejected $f_r(S)$ is

$$P_r(S) = f_r(S) = 1 - \exp(-PN_r(S)) \quad (13A.3.55)$$

where

$$PN_r(S) = V_r \int_0^{\infty} P(r/(a,S)) pn(a) da \quad (13A.3.56)$$

Here V_u is the volume of the component inspected, $P(r/(a,S))$ is the rejection probability, and $pn(a)$ is the preinspection size distribution of flaws. $P(r/(a,S))$ is defined as the probability that a flaw of size (a) will be rejected if the inspection size is set at S and $pn(a)$ is the flaw size distribution. For example, the inspection size S may be defined as the flaw size at which $P(r/(a,S))$ is equal to 0.50, in which case S is written as S_z .

There are several relationships similar to Equations (13A.3.55) and (13A.3.56); some of these follow. Probability material is defective if no inspection is conducted:

$$P_{D0} = 1 - \exp(-PN_{D0})^{SR} \quad (13A.3.57)$$

where

$$PN_{DO} = V_u \int_b^{\infty} pn(a) da \quad (13A.3.58)$$

The probability that sound material will be rejected is

$$P_{SR}(S) = 1 - \exp(-PN_{SR}(S)) (1 - P_{DO}) \quad (13A.3.59)$$

where

$$PN_{SR}(S) = V_u \int_0^b pn(a, S, r) da \quad (13A.3.60)$$

The actual number of rejectable units will be

$$PN_r(S) - PN_{SR}(S) = V_u \left[\int_0^{\infty} pn(a, S, r) da - \int_0^b pn(a, S, r) da \right]$$

Another relationship pertains to the rejectable flaws that pass inspection where

$$P_D(S) = 1 - \exp(-PN_D(S)) \quad (13A.3.61)$$

and

$$PN_D(S) = V_u \int_b^{\infty} P_n((a, S)/\bar{r}) da \quad (13A.3.62)$$

The total number of flaws acceptable and rejectable can be written as

$$PN_{D0} = V_u \left[\int_0^b pn(a) da + \int_b^\infty pn(a) da \right] \quad (13A.3.63)$$

Alternate forms for $PN_r(S)$, $PN_{SR}(S)$ and $PN_D(S)$ follow:

$$PN_r(S) = V_u \int_b^\infty pn(a, S, r) da \quad (13A.3.56a)$$

$$PN_{SR}(S) = V_u \int_0^b p(r/(a, S)) (pn(a)) da \quad (13A.3.60a)$$

or

$$PN_D(S) = V_u \int_b^\infty (1 - P(r/(a, S))) (pn(a)) da \quad (13A.3.62a)$$

Since

$$P_D(S) = 1 - P_r(S) \quad (13A.3.64)$$

$$1 - \exp(-PN_D(S)) = \exp(-PN_r(S)) \quad (13A.3.65)$$

A function containing the characteristic features of rejection probability and analogous to $F(x)$ is

$$P(r/(a, S)) \cong \frac{1}{\delta(S) (2\pi)^{1/2}} \int_{-\infty}^a \frac{\exp-(x-S)^2}{2\pi^2 \delta(S)^2} dx \quad (13A.3.66)$$

where $\delta(S)$ is the inspection uncertainty. If either $\delta(S)$ or $P(r/(a,S))$ and $pn(a)$ are known, the rejection fraction can be determined as a function of inspection size by combining Equations (13A.3.56) and (13A.3.66):

$$PN_r(S) \approx V_u \frac{1}{(2\pi)^{1/2}} \int_0^\infty \frac{pn(a)}{\delta(S)} da \int_{-\infty}^\infty \frac{\exp(-(x-S)^2)}{2\delta^2(S)} dx \quad (13A.3.67)$$

13A.3.4 Failure

The fraction of components which fail is

$$P_f(S) \approx f_f(S) \approx 1 - \exp(-PN_f(S)) \quad (13A.3.68)$$

where

$$PN_f(S) = V_u \int_0^\infty P(f/a) pn((a,S)/\bar{r}) da \quad (13A.3.69)$$

$P(f/a)$ is the probability of failure given a flaw of size (a) exists and $pn((a,S)/\bar{r})$ is defined as equal to the number of flaws existing in the population which passed the inspection with size a to $a + \Delta a$ per unit volume.

Combining

$$PN_f(S) = V_u \int_0^\infty (P(f/a)) (1 - P(r/(a,S))) da \quad (13A.3.70)$$

or

$$(1 - P(r/(a,S))) \approx 1 - \frac{1}{\delta(S)(2\pi)^{1/2}} \int_{-\infty}^a \exp \frac{-(x-S)^2}{2\delta^2(S)} dx \quad (13A.3.71)$$

Substituting Equation (13A.3.70) into Equation (13A.3.68) gives the probable fraction which will fail as a function of inspection size.

13A.3.5 Examples of Flaw Frequency and Flaw Detection

It is apparent that values of $pn(a)$ are essential if the inspection process is to be quantified. Various methods have been used to obtain such data. One such method is destructive examination which requires sectioning sufficient material to count the number of flaws in each size interval. To be statistically significant, this may entail examination of many specimens. An illustration of data obtained in this fashion exists for defects in the 0.1-in. size (pn) (0.1"). In some welds, the value may be as high as 1 in.^{-4} to as low as 10^{-7} in.^{-4} for high quality titanium alloys. The units 10^{-4} are derived from the volume/component times number of components times the length of the flaw size interval of $L^3/N \times N \times L = L^4$ which is the denominator used with the number of flaws in a given interval.

In the case of components such as pressure vessels, it is impossible to establish statistically significant data by destructively sectioning them.

An alternate approach is to remove as many defects discovered by NDE as is feasible to permit their critical evaluation. This involves trepanning sections and replacing with weld metal.

We shall examine the relationships influencing statistically meaningful material--flaw populations.

If x is a given inspection method, with inspection set at S ,

$$f_{rx}(S) \approx 1 - \exp(-PN_{rx}(S)) \quad (13A.3.68a)$$

where

$$PN_{rx}(S) = V_u f_{ro}(S_0) \int_0^\infty \frac{P_x(r/(a,S))}{P_0(r/(a,S_0))} pn_0((a,S_0)/r) da \quad (13A.3.72)$$

A statistically meaningful sample for the pressure vessel would be for the case where, for every significant flaw rejected, there are 19 rejections of no

failure significance. For this example, only 20×10 or 200 trepanned sections need be tested, or about 1,000 pounds versus 2×10^{11} pounds for Case 1, a pure destructive testing program.

13A.3.6 Imperfection Frequency from NDE Indications

While a selective destructive examination such as the one cited in the preceding paragraphs is less expensive than a pure destructive program, it still represents a significant cost.

A third approach is to determine preinspection material quality from the detailed results of NDE. It is necessary first to detect then to evaluate flaws located by NDE. In a nuclear system, detection could proceed in three steps:

1. Determine whether the flaw (either surface or subsurface) is smaller than the flaw standards of ASME XI.
2. If the flaw size exceeds the acceptance standards, conduct a flaw evaluation using the LEFM technique of Appendix A of ASME XI.
3. If the flaw size exceeds the evaluation levels, remove by grinding or by weld repair.

These three steps also entail consideration of flaw growth and continued NDE monitoring in future examinations. It should be noted that rejection implies different things than are typical for small part NDE accept-reject standards.

Using the above ground rules, it should be possible to determine whether the apparent size(s) of the flaw(s) exceed(s) each of the predetermined size(s); e.g., Equation (13A.3.53):

$$P(r/(a,S) = P(r/(D,a,S)) P(D/a) \quad (13A.3.53)$$

The critical parameter as noted in Equation (13A.3.54) is $p_n(a)$. This can be evaluated by several techniques.

For example, $pn(s)$, the distribution of amplitudes observed in the NDE, is related to the amplitude frequency function $pd(S/a)$ and the preinspection material quality $pn(a)$ through the integral equation:

$$pn(S) = \int_0^{\infty} pn(a) pd(S/a) da \quad (13A.3.73)$$

This integral can be deconvoluted to arrive at a measure of $pn(a)$.

The dependence of the various inspection event probabilities upon the decision threshold can be determined if the correlation between the signal response used in the decision and the flaw size (severity) is known. For example, the detection process is usually based on a signal amplitude exceeding some threshold, S_D . The correlation between the signal amplitude and the imperfection size can be described by $pd(S/a)$. Here $pd(S/a) dS$ is the probability that the signal amplitude is between S and $S + dS$, given a flaw size (a).

The flaw detection probability is related to this indication-amplitude flaw-size correlation function and the detection threshold S_D as follows:

$$P(D/a) = \int_{S_D}^{\infty} pd(S/a) dS \quad (13A.3.74)$$

In ASME XI UT procedure (now in ASME V), $S_D = 0.50$ DAC for a standard side-drilled hole.

After detecting the flaw, the decision to "reject" the component may be based on the apparent size (severity) of the flaw exceeding some threshold, a_r . The correlation between the apparent size and the actual size of the flaw can be described by $pd(a/(D,a))$. Here $pd(a/(D,a))da$ is the probability that the apparent size is between a and $a + da$, given the detection event has occurred, and the actual flaw size is (a). This apparent size correlation function and the rejection threshold, a_r , is related to the conditional rejection probability as follows:

$$P(r/(D,a)) = \int_{\hat{a}_r}^{\infty} pd(\hat{a}/(D,a)) d\hat{a} \quad (13A.3.75)$$

This equation may be modified, either because the rejection or detection criteria are not carefully controlled, or because the evaluation should entail an assessment at multiple levels of acceptance/rejection such as the ASME XI flow standards, the acceptable flaw based on LEFM evaluation or the rejectable flaw requiring repairs. In such cases, Equation (13A.3.74) or (13A.3.75) would need to be modified to account for the distribution of $\hat{a}_r$ and S_D and the various levels established for $\hat{a}_r$ and S_D ; e.g., $\hat{a}_{r1}$, $\hat{a}_{r2}$, $\hat{a}_{r3}$ and S_{D1} , S_{D2} , S_{D3} .

One example would be

$$P(r/(D,a)) = \int_0^{\hat{a}_r} d\hat{a}_r (pd(\hat{a}_r) \int_{\hat{a}_r}^{\infty} pd(\hat{a}/D,a) d\hat{a}) \quad (13A.3.76)$$

where $pd(\hat{a}_r)$ describes the distribution of rejection levels.

Returning to the function $pn(\hat{a})$, the apparent size distribution of flaws, $pn(\hat{a})$, which can be determined from the details of the examination record, is related to the preinspection material quality, $pn(a)$, and the apparent flaw size distribution:

$pn(\hat{a})/(D,a)$ through

$$pn(\hat{a}) = \int_0^{\infty} pn(a) P(D/a) pd(\hat{a}/(D,a)) da \quad (13A.3.77)$$

Given $P(D/a)$ $pd(\hat{a}/(D,a))$ and $pn(\hat{a})$, the preinspection quality $pn(a)$ can be determined.

The underlying correlation functions can be estimated directly with a flawed specimen program by recording the distribution of response amplitudes arising from imperfections of a given size range. Such an inspection uncertainty analysis using flawed specimens has two potential advantages over the conventional rejection-nonrejection counting analysis in determining the imperfection rejection probability (IRP):

- With some limited modeling of the inspection process, the IRP at a given inspection level can be estimated with greater confidence from fewer flawed specimens.
- The dependence of the IRP upon the inspection level can be determined from a single inspection of the flawed specimens rather than requiring inspection of the flawed specimens at each inspection level of interest.

Unfortunately, the IRP estimated from a flawed specimen program by either uncertainty analysis or detection counting analysis applies rigorously only to the inspection performance on the geometry, imperfections and environment characteristic of the flawed specimen inspection, and it is very difficult to translate the IRP observed under these artificial conditions to the IRP that describes the actual field or production inspection performance on actual parts with normal production or field imperfections.

The remainder of this section outlines a procedure for determining the underlying correlation functions and, subsequently, the IRP from field or production inspection data rather than from flawed specimen data. This approach avoids the cost of manufacturing intentionally flawed specimens and also avoids the uncertainty associated with how well the imperfections, geometry, and environment in specimen inspections represent actual field or production inspections.

In attempting to use field or production data to generate the underlying correlation functions, the first thing to realize is that the underlying functions (e.g., $pd(s/a)$) cannot be estimated directly from field or production

data because of the difficulty in obtaining an unbiased estimate of the NDE responses for a given imperfection size. What can be obtained from field or production data and destructive examination of selected indications in a given amplitude range is a function (e.g., $pd(a/s)$), which is closely related to the desired correlation function. The desired correlation function, for example, $pd(s/a)$, describes the distribution in NDE response amplitudes "s" associated with a given imperfection size "a" while the field or production observable, for example, $pd(a/s)$, describes the distribution of imperfection sizes "a" associated with a given NDE response "s."

To generate the desired underlying correlation function $pd(s/a)$, both the function $pd(a/s)$ and a second observable $pn(s)$, which describes the frequency of NDE responses at each amplitude, must be determined. Using Equation (13A.3.50), the following relationship can be written between the potential field or production observables $pd(a/s)$ and $pn(s)$ and the desired indication-amplitude, imperfection-size correlation function:

$$pd(s/a) = pd(a/s) pn(s) \left[\int_0^{\infty} pd(a/s) pn(s) ds \right]^{-1} \quad (13A.4.1)$$

Here $pd(a/s)$ is the probability that given an indication of amplitude s , the actual flaw size is between "a" and $a + da$, and $pn(s) ds$ is the probable number of indications per unit volume with amplitude between s and $s + ds$. A comparison of Equations (13A.3.50) and (13A.4.1) indicates a one-to-one correlation between $pd(i/a)$ and $pd(S/a)$.

The observables can be obtained from field or production data. For example, $pn(s)$ can be estimated from a record of the frequency with which indications of different magnitudes occur. Figure 13A.4.1 illustrates the relationship between $pn(s)$ and the number of indications found in production inspection of 1,000 m of weld. This figure illustrates an ultrasonic inspection in which the ultrasonic signal amplitude s is expressed in a distance amplitude corrected (DAC) percent of a signal from a standard reference hole.

FIGURE 13A.4.1. Distribution of Imperfection Detection Response Amplitudes

A histogram of the number of indications in each 10% DAC interval is recorded. An estimate of $pn(s)$ in units of $(\text{meter } \%)^{-1}$ is given by

$$pn(s) = \frac{\text{Number of indications per 10\% interval}}{1,000 \text{ meters} \times 10\%} \quad (13A.4.2)$$

The second observable, $pd(a/s)$, can be estimated by destructive examination of selected indications to determine the actual flaw size that gave rise to the indication. For example, the indications in the vicinity of a 100% DAC and 25% DAC (illustrated in Figure 13A.4.1 by cross hatching) can be destructively examined to obtain an estimate of the distribution of imperfection sizes that correspond to a 100% DAC signal ($pd(a/100\%)$) and the distribution of imperfection sizes that correspond to a 25% DAC signal ($pd(a/25\%)$, respectively. Figure 13A.4.2 illustrates the relationship between $pd(a/100\%)$ and the actual flaw sizes found in the destructive examination of the 22 indications

FIGURE 13A.4.2. Relationship Between $pd(a/s)$ and the Actual Imperfection Sizes Found in Destructive Examination

with ultrasonic signal in the vicinity of 100% DAC; and between $pd(a/25\%)$ and the actual flaw sizes found in the destructive examination of 55 indications in the vicinity of 25% DAC. A histogram of the number of flaws in each 0.1 cm interval with ultrasonic signal amplitude in the vicinity of 100% DAC and 25% DAC are recorded. Estimates of $pd(a/100\%)$ and $pd(a/25\%)$ in units of cm^{-1} are given by

$$pd(a/100\%) = \frac{\text{Number of flaws in size interval } a \text{ with 100\% DAC indications}}{\text{Total number of flaws with 100\% DAC indication} \times 0.1 \text{ cm}} \quad (13A.4.3)$$

and

$$pd(a/25\%) = \frac{\text{Number of flaws in size interval } a \text{ with 25\% DAC indications}}{\text{Total number of flaws with 25\% DAC indication} \times 0.1 \text{ cm}} \quad (13A.4.4)$$

In summary, $pd(a/s)$ and $pn(s)$ can be estimated from field or production data. Using these estimates of $pd(a/s)$ and $pn(s)$ in conjunction with Equation (13A.3.68) gives an estimate of the indication-amplitude, imperfection-size correlation function $pd(s/a)$. Through Equation (13A.3.74), this can be used to estimate the imperfection detection probability and the dependence of the imperfection detection probability on the detection threshold.

In an analogous manner, the apparent-size to actual-size correlation function can be estimated:

$$pd(\hat{a}/D, a) = pd(a/D, \hat{a}) pn(D, \hat{a}) \left[\int_0^{\infty} pd(a/D, \hat{a}) pn(D, \hat{a}) d\hat{a} \right]^{-1} \quad (13A.4.5)$$

Here $pd(a/(D, \hat{a})) da$ is the probability that a detected imperfection of apparent size $\hat{a}$ will have an actual imperfection size between "a" and $a + da$, and $pn(D, \hat{a}) d\hat{a}$ is the probable number of detected imperfections per unit volume of apparent size between $\hat{a}$ and $\hat{a} + d\hat{a}$. These functions can also be estimated from field or production data in a manner analogous to that outlined above. Using these estimates in conjunction with Equation (13A.4.5) gives an estimate of the actual-size to apparent-size correlation function, $pd(a/D, \hat{a})$. This, in turn, through Equation (13A.3.75), can be used to estimate the conditional rejection probability.

In this manner, the imperfection rejection probability can be estimated along with the dependence of this probability upon inspection levels without the introduction of a flawed specimen program.

13A.4.1 Projection from Small-Flaw Frequency

The procedures permit the measurements of $p_n(a)$ for flaw sizes that occur at least occasionally in the manufacture of material units. In some engineering applications, the desired reliability is so high or the number of manufactured material units so low that very few, if any, flaws of concern exist in the available population. Under these conditions, a method is required for projecting $p_n(a)$ to the region of interest from the region where the flaw-frequency is high enough that point estimates can be obtained of $p_n(a)$ by the procedures outlined in the previous subsections; i.e., a method is required for projecting $p_n(a)$ from the range where a is small to the range where a is large.

To arrive at realistic projections, an approximate functional description of $p_n(a)$ is required. Unfortunately, few studies have been conducted in this macroscopic flaw range to determine functions which describe $p_n(a)$. Two functions have been used in the literature: 1) the log--normal function, and 2) the exponential function.

It is essential that systematic studies, including analysis of the underlying causes of the flaw distribution, be conducted to arrive at functions which will describe engineering flaw distributions. Such basic information, in addition to allowing projections to the very low probability regime, would permit more accurate and more rapid estimate of the flaw frequencies in the higher probability regions.

13A.4.2 Determination of the Conditional Failure Probability

To determine the effect of the inspection level change, $P(f/a)$, the probability of failure given that one imperfection of size a exists, must be determined. Three procedures for determining $P(f/a)$ are outlined below: 1) a procedure based on failure history, 2) a procedure based on probabilistic LEFM, and 3) a procedure based on destructive testing.

If a statistically significant failure history is available, the data can give the most accurate estimate of $P(f/a)$; however, such a failure history approach is dependent upon previous relevant history and, hence, cannot be used for a new product or a high reliability product for which no statistically

valid history is available. The failure history can be used to update a new product after it has been in use long enough to establish the relevant history.

Lacking this historical data, a probabilistic fracture mechanics or a destructive testing program on imperfect component samples can be used to determine $P(f/a)$. Such approaches require knowledge or simulation of the spectrum of loads to which the material unit will be subjected in normal use.

13A.4.3 Failure History

In many engineering applications, a history of failures exists. If an analysis of the fracture surface has been conducted to determine the defect size existing at the time of failure, then the number of failures per imperfection size interval divided by the total material volume of the population can be plotted, as illustrated in Figure 13A.4.3, and an estimate of $pn_0(f, a, S_0)$ can be determined. In the limit where Δa approaches zero, $pn_0(f, a, S_0) \Delta a$ is the probable number of imperfections per unit volume with the size between a and $a + \Delta a$ that would cause failure in the life of the material unit after the material unit has passed an inspection method 0 with inspection size set at S_0 .

Having determined the preinspection flaw frequency, $pn_H(a)$, for the historical material and having determined $(P_0/(a, S_0))$, the rejection probability for inspection performed on the population for which the failure data are available, the probable number of imperfections per unit volume between a and $a + \Delta a$ existing inservice is given by $pn_0(a, S_0)/r) \Delta a$, where

$$pn_0((a, S_0)/\bar{r}) = [1 - P_0(r/(a, S_0))] pn_H(a) \quad (13A.4.6)$$

The conditional failure probability is given by

$$p(f/a) = pn_0(f, a, S_0)/pn_0((a, S_0)/\bar{r}), \quad (13A.4.7)$$

FIGURE 13A.4.3. Relationship Between Number of Failure Initiating Flaws and $pn(F, a, S)$

or combining Equations (13A.4.6) and (13A.4.7) gives

$$P(f/a) = \frac{Pn_0(f, a, S_0)}{[1 - P_0(r/(a, S_0))] pn_H(a)} \quad (13A.4.8)$$

The probable fraction of material units which would fail if they passed inspection method X with inspection size set at S is then given by

$$f_{fX}(S) = 1 - \exp(-Pn_{fX}(S)) \quad (13A.4.9)$$

with

$$PN_{fx}(S) = V_u \int_0^{\infty} \frac{1-P_x(r/(a,S))}{1-P_o(r/(a,S))} \frac{pn(a)}{pn_H(a)} pn_o(f,a,S_o) da \quad (13A.4.10)$$

If the present preinspection material quality is the same as the historical and preinspection material quality, then $pn(a) = pn_H(a)$ and $PN_{fx}(S)$ reduces to

$$PN_{fx} = V_u \int_0^{\infty} \frac{1-P_x(r/(a,S))}{1-P_o(r/(a,S))} P_{no}(f,a,S_o) da \quad (13A.4.11)$$

Either Equation (13A.4.10) or (13A.4.11) can be used with Equation (13A.4.9) to calculate the probable fraction of failures as a function of the inspection size when the relevant history is available.

13A.4.4 Fracture Mechanics and Flawed Material Unit Testing

In many engineering applications, the required historical information is not available. Under these conditions, it is necessary either to predict or measure the growth of a flaw under the expected usage. Probabilistic fracture mechanics incorporating when necessary the distribution of initiation time, can give accurate estimates of $P(f/a)$, particularly for applications where the material is in the elastic regime. A number of reviews of fracture mechanics exist in the literature. The fracture mechanics approach is particularly useful because a limited number of tests on laboratory specimens can be used to predict the failure behavior of the specified material under a wide range of load spectrums and material configurations.

Unfortunately, in many material applications, fracture mechanics does not give accurate predictions of $P(f/a)$. Difficulties occur in applications where a) a major fraction of the flawed material life is taken up in crack initiation (actually in crack growth below the size when FM is applicable); b) plastic behavior is important; c) environmental factors are important; or d) because of the complexity of the problem, it may be difficult to calculate the required

stress distributions. In these engineering applications, it may be most effective to conduct, possibly in conjunction with some LEFM analysis, simulation tests directly on a limited number of flawed material units to determine $P(f/a)$.

A semi-quantitative assessment is given in Table 13A.4.1.

13A.4.5 Quantification of Flaw Detection, Location and Sizing

There are relatively limited data available which can be used with the probabilistic equations developed in this report to quantify detection, location and sizing of flaws. The following section is a review of known data plus an exercise in use of the probability equations. It is hoped that this section will indicate both the strengths and the weaknesses of the procedure as well as highlighting the availability or lack of availability of key information.

Four conditions concern us regarding reliability of flaw detection; namely:

	<u>Equipment Reliability</u>	<u>System Reliability</u>
1) Flaw is detected that is present	Reliable	Yes--improves
2) Flaw is detected that does not exist	Unreliable	No effect
3) Flaw is not detected that exists	Unreliable	Adverse
4) Flaw is not detected that does not exist	Reliable	Yes

One and four indicate reliable systems, assuming repair or rejection while two and three represent either a cost penalty (2), or a safety penalty (3). These conditions are illustrated in a Venn diagram, Figure 13A.4.4.

In the case of a nuclear system, the most probable condition is the following:

Flaws exist; their location is unknown; only a portion of the system is examined by NDE; the flaw rate in the uninspected region differs

TABLE 13A.4.1. Size of Flaws Leading to Failure and Conditions

General--For all cases

Stress--Assumed equal to yield (σ_y)

$$\text{Surface stress } \sigma_m = \sigma_b; \sigma_m + \sigma_b = \sigma_y$$

Nominal yield strength 50, 100, 150 Ksi

Nominal values K_{IC} in Ksi $\text{-IN}^{1/2}$ 50, 100, 150

Geometry--plate 10-in. and 2-in. thick

Flaw ratio a/l ; $l = 2c$: 0.50 and 0.1

Flaw location--surface, and at mid-plane:

$$K_{IC} = K_I = \sigma_m M_m \pi^{1/2} a^{1/2} \left(\frac{1}{Q}\right)^{1/2} + \sigma_b M_b (\pi)^{1/2} a^{1/2} \left(\frac{1}{Q}\right)^{1/2}$$

(Cases examined are simplistic, but do indicate sensitivity of critical flaw dimensions to parameters. The surface flaw case is considered to be more accurate than the embedded flaw.)

	Q; $a/l \rightarrow$	Parameters	
		0.5	0.1
	Surface	2.2	0.9
	Subsurface	2.4	1.1

	M_m surface; $a/l \rightarrow$	M_m Values			
		Thickness			
		10 in.		2 in.	
	$a/t < 0.1$	0.5	0.1	0.5	0.1
	$a/t \sim 0.1$	1.1	1.1	1.1	1.1
	$a/t \sim 0.3$	1.1	1.3	1.1	1.3

	M_m subsurface $l \approx a$; $2 l/t = 2 a/t$	10 in.	2 in.
	~ 0.1	1.0	---
	0.15	---	~ 1.0
	0.25	1.05	---
	0.3	---	~ 1.1
	0.55	1.4	---
	0.70	---	1.6

TABLE 13A.4.1. (contd)

		M _b Values			
		Thickness			
		10 in.		2 in.	
M _b surface; a/l →		0.5	0.1	0.5	0.1
a/t < 0.5		1.0	1.1	1.0	1.0
a/t ~ 0.15		---	---	0.9	0.8
a/t ~ 0.3		0.6	0.8	0.8	0.6
M _b subsurface		10 in.		2 in.	
l ≈ a; 2 l/t ≈ 2 a/t					
<0.1		0.1		0.1	
0.15		---		0.15	
0.25		0.3			
0.55		0.5			

Surface Failure Flaw Depth or Half Depth (a)												
Thickness K _{Ic} a/l	10 in.						2 in.					
	50		100		150		50		100		150	
	0.5	0.1	0.5	0.1	0.5	0.1	0.5	0.1	0.5	0.1	0.5	0.1
a	a	a	a	a	a	a	a	a	a	a	a	a
$\frac{\sigma_y}{50}$	0.71	0.24	Leaks	1.4	Leaks	Leaks	>2 in.	0.41	>2 in.	>2 in.	>2 in.	>2 in.
100	0.16	0.06	0.73	0.24	3.24	0.59	0.19	0.06	>2	0.32	>2	>2
150	0.07	0.03	0.30	0.10	0.73	0.25	0.07	0.02	0.47	0.10	>2	0.35
Subsurface												
$\frac{\sigma_y}{50}$	1.8	1.25	NS	NS	NS	NS	>2	NS	>2	NS	>2	NS
100	0.70	0.32	~3.5	1.26	3.5	1.60	NS	0.35	NS	NS	NS	NS
150	0.34	0.15	1.2	0.56	2.5	1.27	0.33	0.15	>2	NS		

NOTE: NS--Not Subsurface.

FIGURE 13A.4.4. Venn Diagram for Conditional Probabilities of Flaw Detection

from the flaw rate in the inspected region. Using a reactor pressure vessel as a surrogate to evaluate the preceding hypotheses, we see that welds are considered to be the most probable location for significant flaws so they are examined essentially completely. While the base metal is examined to a degree during the construction phase, it does not undergo an examination analogous to the preservice and inservice inspections. One reason justifying such an approach is the data indicating that flaw rates should be lower in the base metal (at least for significant flaws.)

The preceding will be recognized as embodied in Equation

$$R_2 = \frac{1}{1 + \frac{\exp \{ \rho \lambda V P_2 (E/F) \} - 1}{P_2 (E/G)}} \quad (13A.3.36)$$

The following are the significant equations used to evaluate flaws quantitatively:

$$pd(S/a) = \frac{pd(a/S)pn(S)}{\int_0^{\infty} pd(a/S)pn(S)dS} \quad (13A.3.41)$$

$$pn((a,S)/\bar{r}) = (1 - P(r/(a,S)))(pn(a)) \quad (13A.3.51)$$

$$pn(a,S,r) = P(r/a,S))(pn(a)) \quad (13A.3.52)$$

$$P(r/(a,S)) = P(r/D,a,S))P(D/a) \quad (13A.3.53)$$

$$pn(a) = \frac{f_{r0}(S_0)pn_0((a,S_0)/r)}{P_0(r/(a,S_0))} \quad (13A.3.54)$$

$$P_r(S) = 1 - \exp(-PN_r(S)) \quad (13A.3.55)$$

$$PN_r(S) = V_u \int_0^{\infty} P(r/(a,S))(pn(a))da \quad (13A.3.56)$$

$$P_{D0} = 1 - \exp(-PN_{D0}) \quad (13A.3.57)$$

$$PN_{D0} = V_u \int_b^{\infty} pn(a)da \quad (13A.3.58)$$

$$P_{SR}(S) = 1 - \exp(-PN_{SR}(S))(1 - P_{D0}) \quad (13A.3.59)$$

$$PN_{SR}(S) = V_u \int_0^b pn(a,S,r)da \quad (13A.3.60)$$

$$P_D(S) = 1 - \exp(-PN_D(S)) \quad (13A.3.61)$$

$$PN_D(S) = V_u \int_b^{\infty} P_n((a,S)/\bar{r}) da \quad (13A.3.62)$$

$$P(r/(a,S)) = \frac{1}{\delta(S)(2\pi)^{1/2}} \int_{-\infty}^a \exp \frac{-(X-S)^2}{2\delta^2(S)} dx \quad (13A.3.66)$$

$$PN_{rx}(S) = V_u f_{ro}(S_0) \int_0^{\infty} \frac{P_x(r/(a,S))}{P_0(r/(a,S_0))} p_{n_0}((a,S_0)/r) da \quad (13A.3.72)$$

$$pn(S) = \int_0^{\infty} pn(a) pd(S/a) da \quad (13A.3.73)$$

$$P(D/a) = \int_{S_D}^{\infty} pd(S/a) dS \quad (13A.3.74)$$

$$P(r/D,a) = \int_{\hat{a}}^{\infty} pd(\hat{a}/(D,a)) d\hat{a} \quad (13A.3.75)$$

$$pn(\hat{a}) = \int_0^{\infty} pn(a) P(D/a) pd(\hat{a}/(D,a)) da \quad (13A.3.77)$$

$$pd(\hat{a}/(D,a)) = \frac{pd(a/(D,\hat{a}))pn(D,\hat{a})}{\int_0^{\infty} pd(a/D,\hat{a}))pn(D,\hat{a}) d\hat{a}} \quad (13A.4.5)$$

13A.4.6 Method of Attack

The available data pertinent to flaw detection, location and sizing can be divided into the following subsets:

- Relevant to detection only
- Relevant to location only
- Relevant to sizing only

- Pertains to detection and location
- Pertains to detection and sizing
- Pertains to location and sizing
- Covers detection, location and sizing.

A limited number of reports are sufficiently comprehensive to permit a re-evaluation of data. Many reports are limited to a few data points, interesting as such, but not capable of re-evaluation. Both the limited information and the more extensive will be reported.

Table 13A.4.2 is an attempt to examine various flaw locations and NDE techniques used for detection in the context of component and material parameters such as surface condition, thickness, geometry, macro- and microstructure. Whereas emphasis is placed on flaw detection, several techniques work well in sizing and, in a few instances, in establishing flaw orientation.

As will be noted from Table 13A.4.2 detection of surface flaws is generally insensitive to thickness, geometry or micro-structure when PT or MT are used; however, the surface condition is critical. With embedded flaws these three parameters of thickness, geometry and micro- or macro-structure become critical, particularly for UT. The following review is an attempt to quantify these truisms and to establish the degree with which each of the cited parameters affect probability of flaw detection.

The ultimate purpose will be to establish some ranking order for the various parameters as functions of NDE technique and flaw location.

Thompson^(13A.4.1) expanded on work of Tittman regarding an approach to quantify the parameters of an unknown defect as a necessary input into a fracture mechanics model. Their approach given in Figure 13A.4.5 complements Figure 13A.4.2. The right side of Figure 13A.4.5 presents the parameters necessary to characterize a flaw and the left side indicates the types of measurements that might be utilized in determining these parameters.

TABLE 13A.4.2. Significant Parameters Influencing Flaw Detection and NDE Techniques Used

Flaw Location	NDE Techniques	Surface Conditions	Component Thickness	Component Geometry	Macro and Micro-Structures	Other as Cited
Surface	MT, PT Visual, Rayleigh ET	Critical ↓	Not important ↓	Not important ↓	Not important ↓	
	UT RT	Not too important	Critical	May be important	Significant, not too important	
Near Surface	MT	Critical ↓	Not important	Not important	Not important	
	ET		Important ↓	May be important ↓	May be important ↓	
	UT					
	RT	Not critical			Not important	Flaw Orientation is critical
Embedded	UT	Important	Significant ↓	May be important ↓	Significant	Flaw Orientation is critical
	RT	Not important	Critical		Not important

FIGURE 13A.4.5. Defect Characterization Scheme

The probability of detecting a flaw with a nondestructive testing procedure is a function of the size, orientation and location of the flaw. The probability of detecting a very small flaw is near zero, whereas the probability of detecting a very large one usually is nearly one. The probability of not detecting a flaw (P_{ND}) is equal to $1 - (\text{probability of detection})$, and is usually the parameter used in analyses. $P_{ND}(a)$ will have an influence on the results of the analysis. P_{ND} will be like a complementary cumulative distribution function in statistical analyses because it varies between 0 and 1, and will only asymptotically approach 0. Hence, candidates for P_{ND} will be drawn from a variety of cumulative distribution functions. The particular distribution can be estimated from information in the literature on P_{ND} for various types of nondestructive techniques. The present analysis will be concerned with both radiographic and ultrasonic inspection techniques, because both typically are used in the inspection of primary piping of power reactors. The initial (preservice) inspection is by both ultrasonics and radiography, and the inservice inspection usually is performed ultrasonically.

13A.5.1 Discrete Distributions

While continuous distributions such as cited in Chapter 11, Sections 3 and 5 are preferred, the detection process lends itself to discrete functions such as the Binomial or the Poisson. O'Neil and Jordan^(13A.5.1) suggested their use in an early study; however, no data were given to validate the choice. Tang^(13A.5.2) suggested the same choice again without data. Packman et al.^(13A.5.3) in the Metals Handbook, Volume 11, 8th Edition, discuss the problem of detection reliability extensively and are known to have substantial data. The binomial distribution was suggested on the basis that NDE is a yes-no, detection-nondetection process. The standard form of the binomial distribution is

$$P(D/F) = \binom{n}{x} P^x (1 - P)^{n-x} \quad (13A.5.1)$$

where n is a positive integer and p has value $0 < p < 1$. p represents the probability of flaw detection and $1 - p$, often given as q , is the probability of nondetection. In this instance, x will be an integer where $0 < x < n$, and

$$\binom{n}{x} = \frac{n!}{x! (n-x)!}$$

The sum of all the positive values for Equation (13A.5.1) is 1 and can be written as

$$[P + (1 - P)]^n = \sum_{x=0}^n \binom{n}{x} P^x (1 - P)^{n-x} = 1 \quad (13A.5.2)$$

The probability of detecting x or more flaws can be found by summing Equation (13.5.1) over all the values S for which $S \geq x$, or

$$P(D/a) = P(S > x) = \sum_{i=x}^n \binom{n}{i} \cdot P^i (1 - P)^{n-i} \quad (13A.5.3)$$

The binomial distribution can be used to determine the error in the detection estimate since there is an appreciable probability that the point estimate is not exactly equal to the true probability of detection. The binomial distribution is the preferred approach for a limited number of nondestructive examinations, the usual case. A standard table of cumulative binomials can be used for a given confidence level. For large values of n and either large (>0.9) or small (<0.1) values of p , the Poisson distribution can be substituted for the binomial. Where suitable, the Poisson parameter is very useful because it tabulates detection or nondetection and is not dependent on n . Packman^(13A.5.3) is suggested for more extensive coverage.

13A.5.2 Continuous Distributions

Several continuous distributions have been suggested for reliability of flaw detection; however, most were mathematical exercises and were not based

on data. I would include Jouris and Shaffer^(13A.5.4) who suggested an exponential and Shaffer^(13A.5.5) who suggested an extreme value maximum distribution in this category.

Troitski^(13A.5.6) used a different approach where flaw size was considered to follow an exponential distribution and prediction of flaw size was the critical parameter. The probability of over-prediction or under-prediction of flaw size was considered to fit a normal (Gaussian) distribution.

Lidiard and Williams^(13A.5.7) and Harrop^(13A.5.8) in extensions of the original work presented in the Marshall report^(13A.5.9) used a modified exponential equation as did Lemon^(13A.5.10) where the probability of nondetection P_{ND} was not allowed to be less than a minimum value regardless of crack size. In the case of References 13A.5.7, 13A.5.8, and 13A.5.9 there were no data. The approach was based on answers to a questionnaire. They assumed the flaw size distribution could be modelled:

$$N(x) = A(x) B(x) \quad (13A.5.4)$$

where $N(x)$ represented the distribution of flaws after fabrication $A(x)$ times the probability defects of a given size were missed, $B(x)$.

$A(x)$ was assumed to fit

$$A(x) = A \exp(-\lambda x) \quad (13A.5.5)$$

$$\lambda = 4.06 \text{ in.}^{-1} \text{ and } A = 14.8 \text{ in.}^{-1}$$

A best estimate of $B(x)$ was

$$B(x) = \epsilon - (1 - \epsilon) \exp(-\mu x) \quad (13A.5.6)$$

$$\epsilon = 0.005 \text{ and } \mu = 2.88 \text{ in.}^{-1}$$

Harrop^(13A.5.8) considered the effect of a series of independent inservice examinations yielding an equation:

$$N_{t_p}^{(P)}(x) = N_{t_p}^{(P-1)}(x) B(x) \quad (13A.5.7)$$

The nondetection probability remained the same.

Harris^(13A.5.11) in an early study examined three continuous distributions -- gamma, Weibull, and log-normal -- using extensive data from NDE of aluminum alloys. In a subsequent study Harris^(13A.5.12) concentrated on a modified log-normal distribution based on detection data in ferritic and austenitic piping.

The following functions were examined in the early study:^(13A.5.11)

$$\text{Weibull } P_{ND}(a) = \exp - (a/\gamma)^n \quad (13A.5.8)$$

$$\text{Gamma} = P_{ND}(a) = \exp - (\mu a) \sum_{n=0}^{k-1} \frac{(\mu a)^n}{n!} \quad (13A.5.9)$$

$$\text{Log-normal } P_{ND}(a) = \frac{1}{2} \operatorname{erfc} (\nu \ln a/a^*) \quad (13A.5.10)$$

$$a^* = \gamma (\ln 2)^{1/2n}$$

where γ , n , μ , k , ν and a^* are adjustable parameters. a^* is the crack size having a 50% chance of being detected; $(P_{ND}(a^*)) = 0.5$. The function $\operatorname{erfc} x$ is the complementary error function. Its relation to the usual form of the log-normal is log-normal cumulative distribution:

$$F(x) = \frac{1}{\sigma(2\pi)^{1/2}} \int_0^x \exp - \frac{1}{2} \frac{(\ln \frac{x-\mu}{\sigma})^2}{\sigma^2} dx \quad (13A.5.11)$$

13A.5.3 The Error Function

The error function is defined as

$$\operatorname{erf} z = \frac{2}{\pi^{1/2}} \int_0^z \exp(-t^2) dt \quad (13A.5.12a)$$

The complementary error function is

$$\operatorname{erfc} z = \frac{2}{\pi^{1/2}} \int_z^\infty \exp(-t^2) dt = 1 - \operatorname{erf} z \quad (13A.5.12b)$$

If

$$t = \frac{(\ln x - \mu)}{\sigma^2^{1/2}}$$
$$F(x) = \frac{1}{2} \left(1 + \operatorname{erf} \frac{(\ln x - \mu)}{\sigma^2^{1/2}} \right) \quad (13A.5.13)$$

The preceding are for the cumulative probability detection; however, it is more common to use the probability of nondetection. Recalling

$$P_D = 1 - P_{ND}$$

then

$$P_{ND} = 1 - P_D$$
$$= 1 - \frac{1}{2} \left(1 + \operatorname{erf} \frac{(\ln x - \mu)}{\sigma^2^{1/2}} \right) \quad (13A.5.14a)$$

$$P_{ND} = 1 - \frac{1}{2} \left[1 + 1 - \operatorname{erfc} \frac{(\ln x - \mu)}{\sigma^2^{1/2}} \right] \quad (13A.5.14b)$$

$$= 1 - \frac{1}{2} \left[2 - \operatorname{erfc} \frac{(\ln x - \mu)}{\sigma^2^{1/2}} \right] \quad (13A.5.14c)$$

$$= 1 - 1 + \frac{1}{2} \operatorname{erfc} \frac{(\ln x - \mu)}{\sigma^2^{1/2}} \quad (13A.5.14d)$$

$$P_{ND} = \frac{1}{2} \operatorname{erfc} \ln \frac{x - \mu}{\sigma^2^{1/2}} \quad (13A.5.14e)$$

Harris^(13A.5.11) uses the form

$$P_{ND} = \frac{1}{2} \operatorname{erfc} \ln v \frac{a}{a^*} \quad (13A.5.10)$$

where

$$\frac{1}{\sigma^2^{1/2}} = v$$

13A.5.4 Lower Bound Detection

Harris^(13A.5.12) in a later study using piping NDE data modified the log-normal equation in line with the approach of Lidiard and Williams:^(13A.5.7)

$$P_{ND} = \epsilon + \frac{1}{2} (1 - \epsilon) \operatorname{erfc} (v \ln a/a^*) \quad (13A.5.11)$$

where ϵ is the lower limit of P_{ND} and other parameters have been defined previously.

Harris^(13A.5.12) used the results of previous studies^(13A.5.9,13A.5.11) together with data of Kupperman et al.^(13A.5.13) to quantify the probability of detecting a flaw of some depth a by various NDE techniques, recognizing

that the results are quite sensitive to factors such as nature of the defect, location in weld or base metal, and grain structure and material, in addition to various NDE equipment variables. The results covered a spectrum of conditions as noted in the following:

- Thick-walled ferritic piping, Reference 13A.5.11
- Thick-walled ferritic pressure vessels, Reference 13A.5.9
- IGSCC in austenitic piping, Reference 13A.5.13.

Figure 13A.5.1 presents detection probabilities from various sources with emphasis on Reference 13A.5.13. A value of 0.005 was selected as the lower limit of P_{ND} for ϵ . In most cases, the 50% probability of detection value for a^* was taken as 0.2 in. (5 mm) and ν was given various values. The best fit to the data occurred for $\nu = 3$ which is suggested for future studies.

The effect of reducing a^* from 0.2 in. (5 mm) to 0.1 in. (2.5 mm) can be seen in Figure 13A.5.2 where the curve marked $a^* = 5$ mm is the same as the $\nu = 3$, $a^* = 5$ mm in Figure 13A.5.1.

13A.5.5 Probability Density Functions

Harris^(13A.5.11) considers both the probability density function (assuming log-normal distribution) and those cracks of concern caused by safety or other considerations. The pdf will be

$$Po(a) = \frac{1}{\mu a (2\pi)^{1/2}} \exp - \frac{1}{2} 2\mu (\ln a/\lambda)^2 \quad (13A.5.16)$$

If the Becher and Hansen^(13A.5.14) data are used, $\lambda = 0.004$ in. and $\mu = 1$. In terms of an error function

$$P_{cond}(>a) = \frac{1}{2} \operatorname{erfc} \left[\frac{1}{\mu 2^{1/2}} \ln a/\lambda \right] \quad (13A.5.17a)$$

where $\frac{1}{\mu 2^{1/2}}$ is the same as $\frac{1}{\sigma 2^{1/2}}$ in Equation (13A.5.10.)

FIGURE 13A.5.1. Detection Probability Data from Reference 13A.5.13 Along with Various Curve Fits to Data and Results from other References

FIGURE 13A.5.2. Nondetection Probability for Various Values of a^*

13A.5.6 Upper Bound Detection

Harris^(13A.5.12) introduced an upper bound value based on pipe wall thickness (t). This is accomplished by subtracting $P_{\text{cond}}(>t)$ and dividing by $1 - P_{\text{cond}}(>t)$ so that $P_{\text{cond}}(>0)$ is still 1 or

$$P_{\text{cond}}(>a) = \frac{\frac{1}{2} \operatorname{erfc} \left[\frac{1}{\mu^{1/2}} \ln a/\lambda \right] - T}{1 - T} \quad (13A.5.17b)$$

where

$$T = \frac{1}{2} \operatorname{erfc} \left[\frac{1}{\mu^{1/2}} \ln t/\lambda \right] \quad (13A.5.17c)$$

13A.5.7 When to Repair

The problem of a flaw size cutoff based on flaws greater than a given size being unsafe or a decision to remove and repair, etc., can be modelled by defining $P_o(a) da$ as the portion of cracks exceeding some limits that fall into the range a to $a + da$. Therefore, the number of cracks of concern of size a to $a + da$ present in a unit volume will be

$$\frac{n(a) da}{n_v(\ell_0)} = P_a(da) \quad (13A.5.18a)$$

and the number of cracks of concern of size a to $a + da$ present in a component of volume V will be given by

$$\frac{n(a) da}{N} = \frac{n(a) da}{V n_v(\ell_0)} = p(a) da \quad (13A.5.18b)$$

where N is the total number of cracks in V , $n_v(\ell_0)$ is the average number of cracks of concern per unit volume and ℓ_0 is some flaw length above which they are rejected.

The probability of having a crack of concern in a component of volume V can be expressed in terms of the probability of having a crack of concern in a unit volume, which is denoted as P_V^* (λ_0) or

$$P^* = 1 - (1 - P_V^*) \quad (13A.5.19)$$

where P^* is the probability of having a crack of size greater than a .

The total as-fabricated crack population can be written as

$$n(a) = V n_V(\lambda_0) \frac{1}{\mu a (2\pi)^{1/2}} \exp \left[-\frac{1}{2\mu^2} (\lambda_n a/\lambda)^2 \right] \quad (13A.5.20)$$

If this function is multiplied by the probability of nondetection

$$P_{ND} = \epsilon + \frac{1}{2} (1 - \epsilon) \operatorname{erfc} (\sqrt{\nu} \ln a/a^*) \quad (13A.5.15)$$

The post-inspection and repair flaw population can be determined.

Another approach suggested by Harris in a later study^(13A.5.12) yields essentially the same results in an equation that can be evaluated by numerical integration. If the assumption is made that all flaws above a given size that are detected are repaired, the post-inspection flaw distribution $P'_0(a)$ can be obtained by multiplying the preservice flaw distribution $P_0(a)$ by the probability of nondetection $P_{ND}(a)$ or

$$P'_0(a) = P_0(a) P_{ND}(a) \quad (13A.5.21)$$

Therefore, the conditional cumulative distribution will be

$$P_{\text{cond}}(>a) = \int_a^{\text{upper}} P_0(a) P_{ND}(a) da \quad (13A.5.22)$$

Here a_{upper} is some upper limit on crack depth such as the critical depth or the wall thickness. If we substitute the expanded equations for P_0 (Equation 13A.5.16) and P_{ND} (Equation 13A.5.15) and taking a_{upper} as h , and integrating we obtain

$$P_{\text{cond}}(>a) = \frac{\epsilon}{2} \left[\operatorname{erfc} \left(\frac{1}{\mu^{1/2}} \ln a/\lambda \right) - \operatorname{erfc} \left(\frac{1}{\mu^{1/2}} \ln h/\lambda \right) \right] + \frac{1-\epsilon}{2\mu\nu (2\pi)^{1/2}} \int_{\nu \ln a/a^*}^{\nu \ln h/a^*} \exp - \frac{(y+\nu \ln a^*/\lambda^*)}{2\mu^2 \nu^2} \operatorname{erfc} y \, dy \quad (13A.5.23)$$

The results of the numerical integration are given in Figure 13A.5.3. The preservice NDE is seen to have a significant influence only for defects larger than a^* which is 0.1 and 0.2 in. (2.5 and 5 mm). It is only when $a > a^*$ that the preservice NDE will have a substantial influence on the crack depth distribution.

The data in Figure 13A.5.3 become the bases for establishing the conditional cumulative failure probability of components as a function of time following the NDE when combined with acceptable initial defect depths. This aspect is addressed in Chapter 12.

13A.5.8 Sensitivity to Model

The early study by Harris compared the Weibull (13A.5.8), Gamma (13A.5.9), and log-normal (13A.5.10) distributions in terms of probability of nondetection of flaws. Figure 13A.5.4 permits a comparison of the distributions using the same data source or the data source modified slightly by adjusting P_{ND} from 1.0 to 0.98 for the smallest flaw and from 0.0 to 0.02 for the largest flaw. The values of the parameters in the equations follow:

$$\text{Weibull } P_{ND}(a) = \exp - (a/\gamma)^\eta$$

$$\gamma = 0.07 \text{ in. and } \eta = 2.0 \quad (13A.5.8)$$

$$a^* = \gamma (\ln 2)^{1/\eta}$$

$$a^* = 0.058 \text{ in.}$$

FIGURE 13A.5.3. Pre- and Post-Inspection Crack Depth Distributions. Post-inspection distributions for the two values of a^* plotted in Figure 13A.5.2 are presented.

FIGURE 13A.5.4. Probability of Nondetection of a Crack as a Function of its Depth for an Ultrasonic Inspection. Data from 3-in. O.D. by 0.25-in. wall aluminum tubing.

$$\text{Gamma } P_{ND}(a) = \exp - (\mu a) \sum_{n=0}^{k-1} \frac{(\mu a)^n}{n!}$$

$$k = 5 \text{ and } \mu = 74 \text{ in.}^{-1}$$
(13A.5.9)

$$\text{Log-normal } P_{ND} = \frac{1}{2} \operatorname{erfc} (v \ln a/a^*)$$

$$v = 1.33 \text{ and } a^* = 0.063 \text{ in.}$$
(13A.5.10)

13A.5.9 Radiographic Model

The same log-normal model was used for reliability of radiographic examination^(13A.5.11) modified for the response of a^* with thickness. This becomes

$$P_{ND}(a) = \frac{1}{2} \operatorname{erfc} (v \ln a/kh) \quad (13A.5.24)$$

where

$$a^* = kh$$

Data from various sources were used to evaluate v and k . These data are plotted in Figure 13A.5.5.

As noted there are substantial scatter. Optimistic value of v and a^* are $v = 2.26$ and $a^* = 0.6 h$ while pessimistic values are $v = 1.53$ and $a^* = h$. The parameter values selected were $P_{ND} = 1/2 \operatorname{erfc} 2.3 \ln a/0.6 h$.

FIGURE 13A.5.5. Probability of non-detection of a crack as a function of its depth for a radiographic inspection.

FIGURE 13A.5.5. Probability of Nondetection of a Crack as a Function of its Depth for a Radiographic Inspection

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CHAPTER 13B

ELASTIC WAVE PROPAGATION AND VELOCITY, SLOWNESS AND WAVE SURFACES

CHAPTER 138

ELASTIC-WAVE PROPAGATION AND VELOCITY, SLOWNESS AND WAVE SURFACES

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CHAPTER 13B

ELASTIC WAVE PROPAGATION AND VELOCITY, SLOWNESS AND WAVE SURFACES

13B.1

INTRODUCTION

Most of the derivations in this chapter are based on work of Musgrave^(13B.1.1), Miller and Musgrave^(13B.1.2) and Auld^(13B.1.3, 13B.1.4).

Those portions dealing with elastic constants and propagation of plane waves in elastic media have been covered in Chapter 9A and are not repeated. The reader may wish to review the relationships in Chapter 9A since prior knowledge is assumed in the derivations presented here.

This chapter will be a vehicle to examine wave behavior in solid media. The interaction of various waves in isotropic and anisotropic media will be examined with respect to the following:

- wave direction
- type of wave - shear, longitudinal
- wave velocity
- stresses and strains developed in various media because of wave propagation
- development of velocity, slowness and wave surfaces
- effect of media on wave characteristics such as beam convergence or divergence.

The preceding is a two-way street. One can calculate structural characteristics such as lattice constants knowing the wave characteristics. Similarly, knowing the lattice constants and type of lattice it is possible to predict beam skewing, etc.

The mathematical bases for each factor (direction, velocity, etc.) will be developed, then combined.

Emphasis in this chapter will be on the wave surface, the velocity surface, and the inverse velocity (slowness) surface. Attention is given to symmetric lattices such as cubics rather than to the more general case of low symmetry lattices. Terminology parallels that in Chapter 9A wherever possible.

Matrix 9A.4.19 yields the three possible wave locations for the three plane waves propagating in a linear elastic homogeneous anisotropic medium. This matrix represents a velocity surface of three sheets. Equations (9A.4.21a) through (9A.4.21c) yield the particle displacement direction cosines for the various plane waves. Musgrave^(13B.1.1) used another development to illustrate wave behavior which follows.

Let

$$S' = \alpha A_x + \beta A_y + \gamma A_z \quad (13B.2.1)$$

$$\alpha S'_x = \alpha^2 A_y + \alpha \beta A_z + \alpha \gamma A \quad (13B.2.1a.1)$$

$$(A - \rho V^2) A_x + \alpha \beta A_y + \alpha \gamma A_z = 0 \quad (13B.2.1a.2)$$

$$\alpha S'_x - \alpha^2 A_y = (\rho V^2 - A) A_x \quad (13B.2.1a.3)$$

$$\alpha S'_x = (\rho V^2 - A + \alpha^2) A \quad (13B.2.1a.4)$$

Similarly for A_y and A_z , and

$$A_x = \frac{\alpha S'}{\rho V^2 - A + \alpha^2} \quad (13B.2.2(a))$$

$$A_y = \frac{\beta S'}{\rho V^2 - B + \beta^2} \quad (13B.2.2(b))$$

$$A_z = \frac{\gamma S'}{\rho V^2 - C + \gamma^2} \quad (13B.2.2(c))$$

Substituting into 13B.2.1,

$$\frac{\alpha^2}{\rho V^2 - A + \alpha^2} + \frac{\beta^2}{\rho V^2 - B + \beta^2} + \frac{\gamma^2}{\rho V^2 - C + \gamma^2} = 1 \quad (13B.2.3)$$

which is another form of matrix 9A.4.19.

Examination of

$$A_x : A_y : A_z = \frac{\alpha}{\rho V^2 - A + \alpha^2} : \frac{\beta}{\rho V^2 - B + \beta^2} : \frac{\gamma}{\rho V^2 - C + \gamma^2} \quad (13B.2.4)$$

shows that direction $A_x : A_y : A_z$ is neither coincident nor orthogonal to $\langle l, m, n \rangle$ (Figure 13B.2.1). Therefore, elastic waves are generally quasilongitudinal or quasitransverse.

Equation (13B.2.4) defines one vector A_1 in Figure 13B.2.1. If we apply equations (13B.2.2a) - (13B.2.2c) to define A_2 , the following equation results; this equation is the general form in solid analytic geometry defining the angle between two lines:

$$A_1 A_2 = A_{x_1} A_{x_2} + A_{y_1} A_{y_2} + A_{z_1} A_{z_2} = \cos A_1 \hat{O} A_2 \quad (13B.2.5)$$

FIGURE 13B.2.1. A Modification of Figure 9A.4.2 Giving the Vector Describing a Normal $\langle l, m, n \rangle$ to a Wave Front Plus the Longitudinal and Shear Waves (A_1, A_2, A_3)

This will yield an equation similar to equation (13B.2.3) with the left hand equal to zero, or the angle is $90^\circ \left(\frac{\pi}{2}\right)$.

The same approach can be used to establish the orthogonality of A_2A_3 and A_3A_1 . Therefore, with any direction $\langle l, m, n \rangle$, there will exist an orthogonal triad of possible displacement vectors each propagating with a certain phase velocity.

If $V_1 = V_2$, vectors A_1 and A_2 will be polarized in the plane of normal A_3 ; however, they are not polarized in any specific direction contained in the plane. For elastic waves, $V_1 = V_2 = V_3$ is not possible.

The general case of the displacement vector of a plane wave of normal $\langle l, m, n \rangle$ and velocity v can be modified for the cubic lattice as follows (terminology in cubic geometry):

Let

$$a = c_{11} - c_{44}$$

$$b = c_{12} - c_{44}$$

$$a-b = c = c_{11} - c_{12} - 2c_{44}$$

$$H = \rho v^2 - c_{44}$$

Using values in equation (9A.6.5a) - (9A.6.5f) and substituting into (9A.4.16) - (9A.4.18), one obtains

$$(l^2 a - H) A_x + l m b A_y + n l b A_z = 0 \quad (13B.2.6(a))$$

$$l m b A_x + (m^2 a - H) A_y + m n b A_z = 0 \quad (13B.2.6(b))$$

$$n l b A_x + m n b A_y + (n^2 a - H) A_z = 0 \quad (13B.2.6(c))$$

yielding the matrix comparable to (9A.4.19):

$$\begin{bmatrix} l^2 a - H & l m b & n l b \\ l m b & m^2 a - H & m n b \\ n l b & m n b & n^2 a - H \end{bmatrix} = 0 \quad (13B.2.7)$$

This matrix may be expanded to develop equations pertinent to specific planar or direction systems, yielding appropriate velocity and displacement vectors. The values by this route will follow those of (9A.5.2) - (9A.5.4). An example of the expanded matrix follows:

For the generalized case of $\langle \ell, m, n \rangle$

$$A_x : A_y : A_z = \frac{\ell}{H_1 - \ell^2 c} : \frac{m}{H_1 - m^2 c} : \frac{n}{H_1 - n^2 c} \quad (13B.2.8)$$

$$= p_1 : q_1 : r_1$$

where p_1, q_1, r_1 define the unit displacement vector.

The expanded matrix^(13B.1.2) will be in the form

$$H^3 - aH^2 + c(a+b) \Sigma H - c^2(a+2b) \pi = 0 \quad (13B.2.9)$$

$$\Sigma = m^2 n^2 + n^2 \ell^2 + \ell^2 m^2$$

$$\pi = \ell^2 m^2 n^2$$

In the simple case of the (001) plane where $z = 0$, the direction number n in the set $\langle \ell m n \rangle$ will be zero, and:

$$\left. \begin{array}{l} A_x \neq 0 \\ A_y \neq 0 \\ A_z = 0 \end{array} \right\} \quad \text{Then } H^2 - aH + c(a+b) \ell^2 m^2 = 0 \quad (13B.2.10)$$

after dividing through by H

Extracting roots

$$H_1 = \frac{1}{2} \left[a \pm \left(a^2 - 4(a+b) \ell^2 m^2 \right)^{1/2} \right]$$

from (13B.2.8)

$$\left(\frac{A_y}{A_x} \right)_i = \frac{H_1 - \ell^2 a}{mb} = \frac{\ell mb}{H_1 - m^2 a}$$

where L = longitudinal or one shear wave S_V with the other shear wave:

$$\left. \begin{array}{l} A_x = 0 = A_y \\ A_z \neq 0 \end{array} \right\} \quad H_{S_H} = 0$$

$$p_{S_H} = 0 = q_{S_H} \quad \text{and} \quad r_{T_H} = 1$$

∴ Wave is truly shear for all $\langle \ell, m, o \rangle$.

For the $\langle 100 \rangle$ and $\langle 110 \rangle$ directions in the (001) plane,

$$\langle 100 \rangle \quad \rho v_L^2 = c_{11}; \quad p_L = 1; \quad q_L = 0 + r_L$$

$$\rho v_{S_H}^2 = c_{44} = \rho v_{S_V}^2; \quad p_{S_H} = 0 = p_{S_V}$$

and the shear displacement vectors lie along any direction contained in the yz plane.

For

$$\langle 110 \rangle \quad \rho v_L^2 = \frac{1}{2} (c_{11} + c_{12} + 2 c_{44}); \quad \rho_L = \frac{1}{\sqrt{2}} = q_L; \quad r_L = 0$$

$$\rho_{V S_H}^2 = c_{44}$$

$$\rho_{S_H} = c = q_{S_H}; \quad r_{S_H} = 1$$

$$\rho_{V S_V}^2 = \frac{1}{2} (c_{11} - c_{12})$$

$$\rho_{S_H} = \frac{1}{\sqrt{2}} = -q_{S_V}; \quad r_{S_V} = 0$$

The preceding are the same values obtained in 9A.5.2 and 9A.5.3.

An examination of the propagation of a uniform plane (bulk) wave along a particular direction as well as the polarization in planes perpendicular to the propagation direction in isotropic and anisotropic media will permit predictions in various lattices.

Values to be used will be the wave vector (K):

$$K = 2\pi/\lambda \quad (13B.3.1)$$

where λ is the wave length and the phase velocity is (V_p) (the propagation velocity of a point of constant phase):

$$V_p = \frac{\omega}{K} \quad (13B.3.2)$$

where ω is the frequency (the number of waves passing any point of the sound field per second).

The propagation of a uniform plane wave in a direction can be designated by the equation

$$u(r,t) = \hat{x} \cos(\omega t - ky) \quad (13B.3.3)$$

where u is the particle displacement field and $\hat{x}$ (or $\hat{y}$ or $\hat{z}$) are unit vectors.

Equation (13B.3.3) is a uniform plane wave of x-polarized particle displacement propagating along the y axis.

The linearized strain-displacement will be

$$\epsilon_{ij}(r,t) = \frac{1}{2} \left(\frac{\partial u_i}{\partial r_j} + \frac{\partial u_j}{\partial r_i} \right) \quad (13B.3.4)$$

where $ij = x, y, z$ are in rectangular coordinates. Therefore, the strain accompanying $u(r,t)$ will be

$$\epsilon_{yx} = \epsilon_{xy} = \frac{1}{2} \frac{\partial u_x}{\partial y} = \frac{K}{2} \sin (\omega t - ky) \quad (13B.3.5)$$

This is an x-polarized, y-propagating shear wave. See Figures 13B.3.1(a) and 13B.3.2(a). Similarly,

$$u = \hat{z} \cos (\omega t - ky); \text{ z-polarized} \quad (13B.3.6)$$

$$u = \hat{y} \cos (\omega t - ky); \text{ y-polarized} \quad (13B.3.7)$$

Figures 13B.3.1(c) and 13B.3.2(c) represent the z-polarized case.

FIGURE 13B.3.1. Grid Diagrams for Plane Uniform Particle Displacement Waves Propagating Along y

FIGURE 13B.3.2. Field Line Diagrams for Uniform Plane Particle Displacement Waves Propagating Along y

For a shear field,

$$\epsilon_{zy} = \epsilon_{yz} = \frac{1}{2} \frac{\partial u_z}{\partial y} = \frac{K}{2} \sin(\omega t - ky) \quad (13B.3.8)$$

Therefore, this is a z-polarized, y-propagating stress wave. In the y-polarized case,

$$\epsilon_{yy} = \frac{\partial u_y}{\partial y} = k \sin(\omega t - ky) \quad (13B.3.9)$$

This is a y-propagating compressional wave. See Figures 13B.3.1(b) and 13B.3.2(b). Pure shear deformation of a cube occurs when forces (F) are applied to the cube faces (Figure 13B.3.3) of area A . If two-dimensional pure

FIGURE 13B.3.3. Traction Forces Applied to a Uniformly Sheared Block

shear (Figure 13B.3.4) is combined with Figure 13B.3.3 three-dimensional pure shear, there will be a static analog of dynamic shear waves where

$$T_{yx} = T_{xy} = F/A \quad (13B.3.10)$$

In the case of a uniform plane wave of xy shear stress propagating along the y axis, the generalized equation for stress in Cartesian coordinates is

$$\frac{\partial^2 T_{ij}}{\partial t^2} = \frac{\rho \partial^2 u_i}{\partial t^2} - F_i \quad (13B.3.11)$$

where i, j, x, y, z , and T_{ij} ; is the stress field. The specific xy shear stress is represented by

$$T_{xy} = T_{yz} = \sin(\omega t - ky) \quad (13B.3.12)$$

In a freely propagating wave with no body force sources, F will be 0, and the translational equation of motion above will satisfy the following conditions:

$$-k \cos(\omega t - ky) = \rho \frac{\partial^2 u_x}{\partial t^2} \quad (13B.3.13)$$

STATIC, PURE SHEAR DEFORMATION OF A CUBE OF MATERIAL IS PRODUCED BY APPLYING FORCES F TO ITS FACES. IF THE AREA OF EACH FACE IS A , THE TRACTION FORCES ON THE $+x$ AND $+y$ FACES AREA:

$$T_x = \hat{y} \frac{F}{A}$$

AND

$$T_y = \hat{x} \frac{F}{A},$$

FIGURE 13B.3.4. Comparison of Simple Shear and Pure Shear. The two cases cannot be distinguished by the strain field alone because one is transformed into the other by a rigid rotation.

The divergence of the xy stress field propagating in the y direction will be

$$\nabla \cdot T = \hat{x} \frac{\partial}{\partial y} T_{xy} = -\hat{x} k \cos(\omega t - ky) \quad (13B.3.14)$$

where $\nabla \cdot T$ is the stress divergence vector also written as

$$\nabla \cdot T = \rho \frac{\partial^2 u}{\partial t^2} \quad (13B.3.15)$$

In a uniform plane wave of xy shear stress propagating along the y axis, the controlling equations will be

$$-\hat{x} k \cos(\omega t - ky) = \rho \frac{\partial^2 u_x}{\partial t^2} \quad (13B.3.16)$$

$$0 = \frac{\partial^2 u_y}{\partial t^2} \quad (13B.3.17(a))$$

$$0 = \frac{\partial^2 u_z}{\partial t^2} \quad (13B.3.17(b))$$

Double integration of

$$\rho \frac{\partial^2 u_x}{\partial t^2} = -k x \cos (\omega t - ky) \quad (13B.3.18)$$

yields

$$u = \hat{x} \frac{k}{\rho \omega^2} \cos (\omega t - ky) \quad (13B.3.19)$$

which is the same as the particle displacement field for the x-polarized, y-propagating shear wave cited previously other than the multiplying factor $k/\rho \omega^2$.

In the strain field,

$$\epsilon_{xy} = \epsilon_{yx} = \frac{1}{2} \frac{k^2}{\rho \omega^2} \sin (\omega t - ky) \quad (13B.3.20)$$

Since the stress field T_{xy} and the propagation field u are both related to the strain field, and the difference is limited to the multiplying factor $k/\rho \omega^2$, the strain field accompanying the modified equation will be

$$\epsilon_{xy} = \epsilon_{yx} = \frac{1}{2} \frac{k^2}{\rho \omega^2} \sin (\omega t - ky) \quad (13B.3.21)$$

Some idea of the implications of these relationships is given in Figure 13B.3.5 where the particles (u) are 180° out of phase with the displacements (T_{xy}).

The preceding stress and strain relationships for a shear wave can be applied to a cubic crystal such as a single crystal of stainless steel.

FIGURE 13B.3.5. Relationship Between the Stress and Particle Displacement Fields in an X-Polarized, Y-Propagating Uniform Plane Wave. Because of mechanical inertia the net forces on the particles in (b) are 180° out of phase with the displacements.

The stress field for the x-polarized, y-propagating shear wave is

$$T_{xy} = \sin(\omega t - ky) \quad (13B.3.12)$$

The strain field is

$$2\epsilon_{xy} = \frac{k^2}{\rho\omega^2} \sin(\omega t - ky) \quad (13B.3.20)$$

A cubic crystal whose coordinate axes (x,y,z) coincided with the crystal axes (X,Y,Z) is given in Figure 13B.3.6.

The Hooke's Law equations in Chapter 9A for a cubic are reduced in matrix notation to the following:

$$\begin{bmatrix} 0 \\ 0 \\ 0 \\ 0 \\ 0 \\ T_{xy} \end{bmatrix} = \begin{bmatrix} c_{11} & c_{12} & c_{12} & 0 & 0 & 0 \\ c_{12} & c_{11} & c_{12} & 0 & 0 & 0 \\ c_{12} & c_{12} & c_{11} & 0 & 0 & 0 \\ 0 & 0 & 0 & c_{44} & 0 & 0 \\ 0 & 0 & 0 & 0 & c_{44} & 0 \\ 0 & 0 & 0 & 0 & 0 & c_{44} \end{bmatrix} \begin{bmatrix} 0 \\ 0 \\ 0 \\ 0 \\ 0 \\ 2\epsilon_{xy} \end{bmatrix}$$

and $T_{xy} = c_{44} T_{xy}^2$

This means that the field defined by $T_{xy}\epsilon_{xy}$ constitutes a valid acoustic wave solution only if

$$c_{44}k^2 = \rho\omega^2$$

The relationship between the wave vector k and the angular frequency ω is called the dispersion relation of the wave. The phase velocity (V_s) for the X-polarized, Y-propagating shear wave in the cubic crystal will be

$$V_s = \frac{\omega}{k} = (c_{44}/\rho)^{1/2} \quad (13B.3.21)$$

FIGURE 13B.3.6. Symmetrically Equivalent Shear Waves in a Cubic Crystal Medium

and the wavelength will be

$$\lambda_s = \frac{2\pi V_s}{\omega} \quad (13B.3.23)$$

Here k is the wave vector which is always proportional to the wave length. The preceding relationships for

$$T_{xy} = 2c_{44} \epsilon_{zy} \quad (9A.10)$$

apply equally to the z-polarized case; namely,

$$T_{zy} = 2c_{44} \epsilon_{zy}$$

and

$$(V_s)_{z\text{-polarized}} = (V_s)_{x\text{-polarized}} = (c_{44}/\rho)^{1/2} \quad (13B.3.22)$$

The x-, y-, z-polarized shear waves in Figure 13B.3.6 propagating along the relevant axes can be combined. Figure 13B.3.7 illustrates how various degenerate shear waves (those propagating along the same direction with the same velocity) may combine to form various particle displacement polarizations. Referring to Figures 13B.3.6 and 13B.3.7, one can combine the x- and z-polarized shear waves propagating along the Y axis:

FIGURE 13B.3.7. Combination of Degenerate Shear Waves to Form Arbitrary Particle Displacement Polarizations

$$U = (\hat{x}A + \hat{z}B) \cos (\omega t - ky) \quad (13B.3.24)$$

This combination yields a shear wave with the same phase velocity $(\rho/C_{44})^{1/2}$; however, there will be a linearly polarized particle displacement along the direction $xA + zB$ (Figure 13B.3.7(c)).

A different case is one where the polarized waves combine with equal amplitudes but with a 90° phase shift:

$$U = \hat{x} \cos (\omega t - ky) + \hat{z} \sin (\omega t - ky) \quad (13B.3.25)$$

This will yield a circularly polarized particle displacement; e.g.,

$$u = (U_x^2 + U_z^2)^{1/2} \quad (13B.3.26)$$

$$\tan \chi = U_z/U_x = \tan (\omega t - ky) \quad (13B.3.27)$$

(See Figure 13B.3.7(d).) Similar approaches will yield an elliptically polarized pattern (Figure 13B.3.7(e)).

It is apparent on symmetry grounds that the same relationships apply to shear waves travelling along the X and Z crystal axes. Indeed, travel along the $\langle 100 \rangle$ family of directions yields the same phase velocity $(c_{44}/\rho)^{1/2}$.

Compressional wave propagation in a cubic axis with a y -propagating compressional wave is represented by

$$u = \hat{y} \cos (\omega t - ky) \quad (13B.3.7)$$

$$\sigma_{yy} = k \sin (\omega t - ky) \quad (13B.3.28)$$

For the case of a cubic single crystal where the coordinate axes coincide with the crystal axes,

$$\begin{bmatrix} \sigma_{xx} \\ \sigma_{yy} \\ \sigma_{zz} \\ T_{yz} \\ T_{zx} \\ T_{xy} \end{bmatrix} = \begin{bmatrix} c_{11} & c_{12} & c_{12} & 0 & 0 & 0 \\ c_{12} & c_{11} & c_{12} & 0 & 0 & 0 \\ c_{12} & c_{12} & c_{11} & 0 & 0 & 0 \\ 0 & 0 & 0 & c_{44} & 0 & 0 \\ 0 & 0 & 0 & 0 & c_{44} & 0 \\ 0 & 0 & 0 & 0 & 0 & c_{44} \end{bmatrix} \begin{bmatrix} 0 \\ \epsilon_{yy} \\ 0 \\ 0 \\ 0 \\ 0 \end{bmatrix}$$

$$\sigma_{xx} = c_{12} \epsilon_{yy} = c_{12} k \sin(\omega t - ky) \quad (13B.3.29(a))$$

$$\sigma_{yy} = c_{11} \epsilon_{yy} = c_{11} k \sin(\omega t - ky) \quad (13B.3.29(b))$$

$$\sigma_{zz} = c_{12} \epsilon_{yy} = c_{12} k \sin(\omega t - ky) \quad (13B.3.29(c))$$

The equations of motion following are for a free wave propagating along the y axis:

$$\frac{\partial}{\partial y} T_{xy} = -\rho \omega^2 u_x \quad (13B.3.30(a))$$

$$\frac{\partial}{\partial y} \sigma_{yy} = -\rho \omega^2 u_y \quad (13B.3.30(b))$$

$$\frac{\partial}{\partial y} T_{zy} = -\rho \omega^2 u_z \quad (13B.3.30(c))$$

Only σ_{yy} is common to the two sets of equations. Therefore,

$$-\rho \omega^2 u_x = 0 \quad (13B.3.31(a))$$

$$-\rho \omega^2 u_y = \frac{\partial}{\partial y} \sigma_{yy} = -c_{11} k^2 \cos(\omega t - ky) \quad (13B.3.31(b))$$

$$\rho \omega^2 u_z = 0 \quad (13B.3.31(c))$$

Substituting for U_y ,

$$-\rho\omega^2 \cos(\omega t - ky) = -c_{11}k^2 \cos(\omega t - ky) \quad (13B.3.32)$$

or

$$\rho\omega^2 = c_{11}k^2 \quad (13B.3.33)$$

The phase velocity (v_ℓ) for a compressional^(a) wave propagating along the Y cubic crystal axis will be

$$v_\ell = \frac{\omega}{k} = (c_{11}/\rho)^{1/2} \quad (13B.3.34)$$

This relationship applies to the family $\langle 100 \rangle$. In compressional^(a) or shear^(a) waves in cubic crystals in other directions, proof of the controlling equations for families of axes other than $\langle 100 \rangle$ becomes quite complicated. For example, with $\langle 110 \rangle$ or $\langle 111 \rangle$ directions, the options consist either of a rotation of axes or quite complicated analytic solutions of equations.

If one leaps from the model to the solution, the results are as follows:

Compressional wave $[110]$

$$v_\ell[110] = \left(\frac{c_{11} + c_{12} + 2c_{44}}{2\rho} \right)^{1/2} \quad (13B.3.35)$$

Shear wave $[110]$

$$v_s[110] = \left(\frac{c_{11} - c_{12}}{2\rho} \right)^{1/2} \quad (13B.3.36)$$

(a) Alternate terminology is longitudinal for compressional and transverse for shear.

$$v_s[001] = \left(\frac{c_{44}}{\rho} \right)^{1/2} \quad (13B.3.37)$$

In the case of shear waves, the velocities will differ depending on the polarization direction; whether [110] or [001]. This contrasts with shear waves propagating along $\langle 100 \rangle$ axes.

Hexagonal crystals are of interest because of their uniformity about the z-axis. This orthotropic symmetry is similar (in two-dimensions) to isotropic symmetry. When propagation is in the XY plane (normal to Z-axis), there are two pure shear and one pure longitudinal mode; namely,

$$(v_s)_{y\text{-polarized}} = \left(\frac{c_{66}}{\rho} \right)^{1/2} = \left(\frac{c_{11} - c_{12}}{2\rho} \right)^{1/2} \quad (13B.3.38)$$

$$(v_s)_{z\text{-polarized}} = \left(\frac{c_{44}}{\rho} \right)^{1/2} \quad (13B.3.39)$$

$$(v_L)_{\text{compressional}} = \left(\frac{c_{11}}{\rho} \right)^{1/2} \quad (13B.3.40)$$

Since hexagonal crystals are anisotropic, the above represents a case where the complete set of acoustic waves are trirefringent.

Behavior in the basal plane is more apparent in inverse-velocity (slowness) diagrams discussed later.

In isotropic media, the phase velocity described in 13B.1 and Figure 13B.1.1 defines ray (or wave), normal (or velocity), and inverse (or slowness) surfaces where these surfaces are defined as follows:

- Ray (or Wave) Surface – the plot of energy velocity versus energy flow direction; the envelope of planes normal to the phase velocity (V_p)(k vector).
- Normal (or Velocity) Surface – the plot of phase velocity (V_p) versus the vector (k).
- Slowness (Inverse Velocity) Surface – the inverse of the phase velocity ($k/\omega = 1/V_p$) as a function of the propagation direction. It is independent of the frequency (ω).

Following are a few other relevant definitions:

- Wave Surface Vector – the sum of the velocity vector plus the symmetry vector.
- Phase Front – phase fronts of plane waves are normal to the wave vector (k); therefore, each portion of the ray surface corresponds to the phase front for a plane wave with energy traveling in that direction.
- Energy Velocity – defined in terms of the average power flow density, or

$$V_e = P_{avg}/U_{avg}; \text{ alternately, } \hat{k}V_e = V_p$$

The vector

$$\hat{k}V_e = V_p = V_e \cos \psi \quad (13B.4.1)$$

The latter relationship is significant in anisotropic media where k and V_e will not necessarily have the same direction. Therefore, a plot of V_e as a function of its direction, yields the ray surface, which cannot be obtained

through a simple inversion of the slowness surface. Figure 13B.4.1 permits a comparison of the energy velocity (V_e) and the inverse phase velocity ($k/\omega = 1/V_p$). As noted, the energy velocity (V_e) is always normal to the slowness surface (Figure 13B.4.1(a)). The wave vector k , similarly, is normal to the ray surface (Figure 13B.4.1(b)). Also, Figure 13B.4.1(c) illustrates the relationship between the phase velocity V_p and the energy velocity V_e .

Figure 13B.4.2 permits a clearer interpretation of phase velocity, energy velocity, the wave vector (k), the normal surface, and the ray (wave) surface. The wave vector is always normal to the ray surface. As noted,

$$V_p = V_e \cos \psi = \hat{k} \cdot V_e \quad (13B.4.1)$$

The normal surface (a plot of phase velocity (V_p) versus wave vector direction) and the ray surface (a plot of energy velocity versus energy flow direction) are illustrated (Figure 13B.4.2). The ray surface is the envelope of planes normal to V_p . Since the phase fronts of a plane wave are normal to k , each portion of the ray surface corresponds to the phase front for a plane wave with energy traveling in that direction.

The velocity of the modulation on a wave is called the group velocity (which in a rigorous sense applies only to lossless media). However, it can be approximated with a one-dimensional modulation envelope constructed by taking two waves with slightly differing values of ω and k :

$$\cos(\omega t - kz) - \cos[(\omega + \delta\omega)t - (k + \delta k)z] = \quad (13B.4.2)$$

$$2 \cos \left[\left(\omega + \frac{\delta\omega}{2} \right) t - \left(k + \frac{\delta k}{2} \right) z \right] \cos \left(\frac{\delta\omega t}{2} - \frac{\delta k z}{2} \right)$$

carrier wave modulation envelope

The propagation velocity of the carrier is the phase velocity (V_p):

$$V_p = \frac{\omega + \delta\omega/2}{k + \delta k/2} \approx \frac{\omega}{k} \quad (13B.4.3)$$

The propagation velocity of the modulation envelope is the group velocity (V_g):

$$V_g = \frac{\delta \omega}{\delta k} \rightarrow \frac{\partial \omega}{\partial k} \quad (13B.4.4)$$

The dispersion relation for plane acoustic waves is

$$|k^2 \Gamma_\omega(\hat{i}) - \rho \omega^2| = \Omega(\omega, k_x, k_y, k_z) = 0 \quad (13B.4.5)$$

Differentiating,

$$\left(\frac{\partial \Omega}{\partial \omega} \delta \omega + \frac{\partial \Omega}{\partial k_x} \delta k_x \right)_{k_y, k_z} = 0 \quad (13B.4.6)$$

or

$$\left(\frac{\partial \omega}{\partial k_x} \right)_{k_y, k_z} = - \frac{\partial \ell / \partial k_x}{\partial \ell / \partial \omega}, \text{ etc.} \quad (13B.4.7)$$

and

$$V_g = \frac{-V \Omega}{\partial \Omega / \partial \omega} \quad (13B.4.8)$$

Then V_g and V_e will be identical for acoustic waves since group velocity has a directly measurable physical meaning not apparent in the definition of energy velocity.

Figure 13B.4.3 pictorially represents the real case of a pulse of acoustic energy radiated by a plane-wave transducer; the wave packet, which is limited in two dimensions by the size of the transducer and in the third dimension by the pulse length, travels along the path noted. The wave fronts travel along the direction k , which is normal to the transducer surface. The wave packet modulation envelope travels in the direction $V_g = V_e$; as noted, this may be at

FIGURE 13B.4.3. Deflected Acoustic Beam Trajectory in an Anisotropic Solid

an angle with respect to k . Therefore, the receiving transducer will need to be offset to intercept the acoustic pulse.

The geometrical relationships between velocity (normal), inverse (slowness) and wave (ray) vectors can be seen more clearly in Figure 13B.4.4 for both longitudinal and shear waves. The discussion will concentrate on the longitudinal waves, Figure 13B.4.4(a), recognizing that the same arguments apply to shear waves, Figure 13B.4.4(b). The geometrical relationships should clarify the analytic equations that follow.

Definitions

Line $\langle \ell, m, n \rangle$ - the inverse surface of three sheets is the locus of points defined by a radius vector of magnitude c_{44}/ρ_v and direction $\langle \ell, m, n \rangle$

where $A' = c_{44}/\rho_v$ (A' will be discussed later in 13B.5.1.)

λ_L, μ_L, ν_L - the direction cosines of the line joining Inverse Point I to the corresponding wave point P, or (P is a point on the wave surface), and

$$\lambda_L : \mu_L : \nu_L = \frac{p_L^2}{\ell} : \frac{q_L^2}{m} : \frac{r_L^2}{n} \quad (13B.4.9)$$

* T = TRANSVERSE (SHEAR)

FIGURE 13B.4.4. Geometrical Relation Between Velocity, Inverse, and Wave Vectors: (a) $v > c_{44}/\rho v$ as for L Waves; (b) $v < c_{44}/\rho v$ as for T Waves.

The angle ϵ_L is given by

$$\cos \epsilon_L = \ell \lambda_L + m \mu_L + n \nu_L = \left[\frac{p_L^4}{\ell^2} + \frac{q_L^4}{m^2} + \frac{r_L^4}{n^2} \right]^{-1/2} \quad (13B.4.10)$$

Points on the wave surface are defined as

$$\epsilon_L = \frac{\ell c_{44}}{\rho v_L} + \frac{\rho_L^2 H_L}{\ell \rho v_L}; \quad \eta_L = \frac{m c_{44}}{\rho v_L} + \frac{q_L^2 H_L}{m \rho v_L}; \quad \zeta_L = \frac{n c_{44}}{\rho v_L} + \frac{r_L^2 H_L}{n \rho v_L} \quad (13B.4.11)$$

Similar equations apply to shear waves.

Points on the wave surface are defined as

$$(138.4.11) \quad \frac{\partial \phi}{\partial x} = \frac{\partial \phi}{\partial x} + \frac{\partial \phi}{\partial y} + \frac{\partial \phi}{\partial z} = 0; \quad \frac{\partial \phi}{\partial x} = \frac{\partial \phi}{\partial x} + \frac{\partial \phi}{\partial y} + \frac{\partial \phi}{\partial z} = 0; \quad \frac{\partial \phi}{\partial x} = \frac{\partial \phi}{\partial x} + \frac{\partial \phi}{\partial y} + \frac{\partial \phi}{\partial z} = 0$$

Similar equations apply to shear waves.

13B.5.1 The Wave Surface Equations

To obtain the wave surface, it is necessary to find the envelope of planes

$$l\xi + m\eta + n\zeta - v = 0 \quad (13B.5.1)$$

subject to the conditions

$$l^2 + m^2 + n^2 = 1 \quad (13B.5.2)$$

and

$$\frac{\alpha^2}{\rho v^2 - A + \alpha^2} + \frac{\beta^2}{\rho v^2 - B + \beta^2} + \frac{\gamma^2}{\rho v^2 - C + \gamma^2} = 1 \quad (13B.2.3)$$

The differentials for the above relations are

$$\xi dl + \eta dm + \zeta dn - dv = 0 \quad (13B.5.3)$$

$$l dl + m dm + n dn = 0 \quad (13B.5.4)$$

$$\left[\frac{\left\{ \frac{\partial \alpha^2}{\partial l} (\rho v^2 - A) + \alpha^2 \frac{\partial A}{\partial l} \right\}}{(\rho v^2 - A + \alpha^2)^2} + \frac{\left\{ \frac{\partial \beta^2}{\partial l} (\rho v^2 - B) + \beta^2 \frac{\partial B}{\partial l} \right\}}{(\rho v^2 - B + \beta^2)^2} + \frac{\left\{ \frac{\partial \gamma^2}{\partial l} (\rho v^2 - C) + \gamma^2 \frac{\partial C}{\partial l} \right\}}{(\rho v^2 - C + \gamma^2)^2} \right] dl$$

$$+ \left[\frac{\left\{ \frac{\partial \alpha^2}{\partial m} (\rho v^2 - A) + \alpha^2 \frac{\partial A}{\partial m} \right\}}{(\rho v^2 - A + \alpha^2)^2} + \frac{\left\{ \frac{\partial \beta^2}{\partial m} (\rho v^2 - B) + \beta^2 \frac{\partial B}{\partial m} \right\}}{(\rho v^2 - B + \beta^2)^2} + \frac{\left\{ \frac{\partial \gamma^2}{\partial m} (\rho v^2 - C) + \gamma^2 \frac{\partial C}{\partial m} \right\}}{(\rho v^2 - C + \gamma^2)^2} \right] dm$$

$$\begin{aligned}
& + \left[\frac{\left\{ \frac{\partial \alpha^2}{\partial n} (\rho v^2 - A) + \alpha^2 \frac{\partial A}{\partial n} \right\}}{(\rho v^2 - A + \alpha^2)^2} + \frac{\left\{ \frac{\partial \beta^2}{\partial n} (\rho v^2 - B) + \beta^2 \frac{\partial B}{\partial n} \right\}}{(\rho v^2 - B + \beta^2)^2} + \frac{\left\{ \frac{\partial \gamma^2}{\partial n} (\rho v^2 - C) + \gamma^2 \frac{\partial C}{\partial n} \right\}}{(\rho v^2 - C + \gamma^2)^2} \right] dn \\
& - \left[\frac{\alpha^2}{(\rho v^2 - A + \alpha^2)^2} + \frac{\beta^2}{(\rho v^2 - B + \beta^2)^2} + \frac{\gamma^2}{(\rho v^2 - C + \gamma^2)^2} \right] 2\rho v dv = 0 \quad (B13.5.5)
\end{aligned}$$

Equations (13B.2.2(a)) through (13B.2.2(c)) yield

$$\frac{1}{(\rho v^2 - A + \alpha^2)^2} = \frac{A_x^2}{\alpha^2 S'^2} \quad (13B.5.6(a))$$

$$\frac{1}{(\rho v^2 - B + \beta^2)^2} = \frac{A_y^2}{\beta^2 S'^2} \quad (13B.5.6(b))$$

$$\frac{1}{(\rho v^2 - C + \gamma^2)^2} = \frac{A_z^2}{\gamma^2 S'^2} \quad (13B.5.6(c))$$

Using undetermined multipliers - A' with (13B.5.4) and B' with (13B.5.5),

$$\xi - A' + B'P = 0 \quad (13B.5.7)$$

$$\eta - A'm + B'Q = 0 \quad (13B.5.8)$$

$$\zeta - A'n + B'R = 0 \quad (13B.5.9)$$

$$1 + \frac{2\rho v B'}{S'^2} (A_x^2 + A_y^2 + A_z^2) = 0 \quad (13B.5.10)$$

where P, Q, R are written for the coefficients of $d\ell$, dm , dn in 13B.5.5.

Multiplying 13B.5.7 by ℓ , 13B.5.8 by m , 13B.5.9 by n , and adding

$$\ell \xi + m \eta + n \zeta - A' = v - A' = B' (\ell P + m Q + n R) \quad (13B.5.11)$$

Substituting (13B.5.6(a)) through (13B.5.6(c)),

$$P = \frac{1}{S^2} \left[\frac{A_x^2}{\alpha^2} \left\{ \frac{\partial \alpha^2}{\partial \ell} (\rho v^2 - A) + \alpha^2 \frac{\partial \rho}{\partial \ell} \right\} + \frac{A_y^2}{\beta^2} \left\{ \frac{\partial \beta^2}{\partial \ell} (\rho v^2 - B) + \beta^2 \frac{\partial \rho}{\partial \ell} \right\} \right. \\ \left. + \frac{A_z^2}{\gamma^2} \left\{ \frac{\partial \gamma^2}{\partial \ell} (\rho v^2 - C) + \gamma^2 \frac{\partial \rho}{\partial \ell} \right\} \right] = \frac{1}{S^2} \left[\frac{A_x^2}{\alpha^2} A_\ell + \frac{A_y^2}{\beta^2} B_\ell + \frac{A_z^2}{\gamma^2} C_\ell \right]$$

with 13B.5.10,

$$P = - \frac{1}{2\rho v B^2} \left[\frac{p^2}{\alpha^2} A + \frac{q^2}{\beta^2} B + \frac{r^2}{\gamma^2} C \right] = - \frac{1}{2\rho v B^2} L' \quad (13B.5.12)$$

where

$$p:q:r = \frac{A_x:A_y:A_z}{(A_x^2 + A_y^2 + A_z^2)^{1/2}} \quad (13B.5.13)$$

The value of A' for cubic and hexagonal symmetries is selected as

$$A' = c_{44}/\rho v, \text{ i.e., } k = c_{44}$$

This is the locus of a surface. For any value of $A' \neq 0$, there will exist for any medium, velocity, index, (inverse velocity) and wave surface a set of equations such as given in (9A.4.16 - 9A.4.18). The index (slowness) surface in the inverse of the velocity surface with respect to a sphere of inversion whose radius is directly related to A' .

As noted in Figure 13B.4.4, both vectors OP and IP define the wave surface. Redefining,

Wave Surface – the polar reciprocal with respect to the inversion sphere of the index (slowness) surface; also it is the first negative pedal surface of the velocity surface.

Suggested visualization of ultrasonic wave is as a polarized source of single frequency whose dimensions are large compared to the wave length of the elastic waves propagated. If the UT source is large compared to the wave length, a diverging wave front will be emitted.

The first energy reaching the receiving side will be that arriving on the plane element tangent to the surface; it will have traveled across the sample with the phase velocity (V_p) along $\langle \ell, m, n \rangle$ appropriate for such displacement. In general, the imposed displacement will be resolved along the directions A_1, A_2, A_3 , and the resolved components of displacements will travel with three discrete velocities appropriate to plane waves of normal $\langle \ell, m, n \rangle$. Similarly,

$$Q = - \frac{1}{2\rho v B^T} \left[\frac{p^2}{\alpha^2} A_m + \frac{q^2}{\beta^2} B_m + \frac{r^2}{\gamma^2} C_m \right] = - \frac{1}{2\rho v B^T} M \quad (13B.3.14)$$

$$R = - \frac{1}{2\rho v B^T} \left[\frac{p^2}{\alpha^2} A_n + \frac{q^2}{\beta^2} B_n + \frac{r^2}{\gamma^2} C_n \right] = - \frac{1}{2\rho v B^T} N \quad (13B.3.15)$$

so that Equation (13B.5.11) becomes

$$v - A' = \frac{1}{2\rho v} (\ell L + m M + n N). \quad (13B.5.16)$$

Thus the coordinates of any point on the wave surface are

$$\xi = \ell \left\{ v - \frac{1}{2\rho v} (\ell L + m M + n N) \right\} + \frac{L}{2\rho v} \quad (13B.5.17)$$

$$\eta = m \left[v - \frac{1}{2\rho v} (\ell L + m M + n N) \right] + \frac{M}{2\rho v} \quad (13B.5.18)$$

$$\zeta = n \left[v - \frac{1}{2\rho v} (\ell L + m M + n N) \right] + \frac{N}{2\rho v} \quad (13B.5.19)$$

and ξ, η, ζ are direction numbers for the path of energy or ray corresponding to a wave normal $\langle \ell, m, n \rangle$, Figure 13B.4.4. Combining these three,

$$r = v + \frac{1}{2\rho v} \begin{bmatrix} (1-\ell^2) & \ell m & n \ell \\ \ell m & (1-m^2) & m n \\ n \ell & m n & (1-n^2) \end{bmatrix} \begin{bmatrix} L \\ M \\ N \end{bmatrix} \quad (13B.5.20)$$

Therefore, the wave-surface vector is equal to the velocity vector plus another vector which is related in a more or less complicated manner to the structural symmetry of the medium, depending on the order of symmetry.

The preceding analytic equations are directly applicable to cubic crystals either as single crystals or in highly oriented coarse-grained structures. Recognizing that it is difficult to visualize geometries from the equations, several figures are included to permit an assessment of both velocity and wave surfaces. Figures 13B.5.1 and 13B.5.2 illustrate velocity-surface intersections with the (001) and (110) planes. Figure 13B.5.3 permits one to examine changes as the plane is rotated in 15° increments from (100) to (110). At certain loci such as the x, y, or z axes, the quasilongitudinal or quasishear waves revert to pure longitudinal or pure shear.

Figures 13B.5.4 and 13B.5.5 illustrate wave (ray) surfaces for (100) and (110) planes similar to velocity surfaces in Figures 13B.5.1 and 13B.5.2. Obviously, the wave surfaces are very complex. Recalling that the angle ψ becomes zero when the surfaces are pure longitudinal or shear, the velocity and wave surfaces must come into contact at all points where ψ is zero.

FIGURE 13B.5.1. Velocity-Surface Intersection with (100) Planes for (1) Single-Crystal Stainless Steel; (2) Nickel (Analytically); (3) Iron (Analytically); and (4) 316 SS Weld Metal

Recalling the relationships between V_e and k/ω for slowness surfaces and k and V_e for wave surfaces (Figures 13B.4.1 and 13B.4.2), both V_e and k are normal to the slowness and wave surfaces, respectively; and reexamining the relationship of the vector denoted by OP and IP in Figure 13B.4.4, it is apparent that wave surfaces can be developed from either slowness or velocity surfaces. In fact, the slowness surface is much more powerful. The cusps in the shear-wave surface are directly related to the nodes in the slowness surface. This can be seen by examining velocity (normal), slowness, and wave surfaces for the same crystal. Figure 13B.5.6 permits this comparison.

FIGURE 13B.5.2. Velocity-Surface Intersection with (110) Plane for (1) Single Crystal Stainless Steel; (2) Dendritic Structure of Stainless Steel; (3) Nickel (Analytically)

If the velocity and wave surfaces are overlaid for the most complex case of shear wave, vertically polarized Figure 13B.5.6(b) results.

The preceding slowness surface serves as an introduction to the next section on that topic.

13B.5.2 Slowness (Inverse Velocity) Surfaces

The propagation characteristics in isotropic and anisotropic solids can be determined in terms of Hookes Law (Chapter 9A - Equations 9A.4.10 - 9A.4.15). These equations are generalized to

$$\Omega(\omega, k_x, k_y, k_z) = /k^2 \Gamma_1(\ell_x, \ell_y, \ell_z) - \rho\omega^2\delta_i/ = 0 \quad (13B.5.21)$$

FIGURE 13B.5.3. Orientation Dependence of Velocity in Four Crystal Planes Presented in Polar Coordinates for 316 SS Weldment:

At fixed ω , a surface is defined in k -space as a function of its direction. This is called the wave vector surface. The first term contains k^2 and the second ω^2 , so the relation can be expressed in terms of the ratio k/ω . This means the wave vector k is always proportional to ω .

FIGURE 13B.5.4. Wave-Surface Intersection with the (001) Plane for
 (1) Single-Crystal Stainless Steel; (2) Nickel

FIGURE 13B.5.5. Wave-Surface Intersections With the (110) Plane for (1) Single Crystal Stainless Steel; (2) Dendritic Structure of Stainless Steel; (3) Nickel

It may be more convenient to consider the inverse of phase velocity $1/V_p = k/\omega$ as a function of the propagation direction. The slowness surface is a powerful tool in interpreting the behavior of isotropic, orthotropic and anisotropic solids.

FIGURE 13B.5.6. Illustration of (a) Points on Slowness Surface Common to Wave Surface and (b) Sections of the Normal and Wave Surfaces in the XY Plane of a Cubic Crystal (Nickel)

The three cases of interest are isotropic, cubic and hexagonal. A relatively fine-grained steel behaves isotropically. As noted in Chapter 9A, for Lamé constants,

$$\text{Isotropic} = V_p = \omega/K \text{ (independent of direction)}$$

$$V_L = (c_{11}/\rho)^{1/2} = \left(\frac{\lambda + 2\mu}{\rho} \right)^{1/2} \quad (9A.6.2(a))$$

$$V_S = (c_{44}/\rho)^{1/2} = (\mu/\rho)^{1/2} \quad (9A.6.2(b))$$

The inverses will give the slowness surface which will consist of two concentric spheres (Figure 13B.5.7(b)) with the outer sphere shear waves and the inner longitudinal waves.

FIGURE 13B.5.7. (A) Acoustic Plane Wave Scattering at a Plane Boundary Between Two Isotropic Media; (B) Isotropic Slowness Surface Illustrating Controlling Inverse Velocity Equations

A cubic may have a pure shear wave, a quasishear wave and a quasi-longitudinal wave.

The pure shear wave is polarized normal to the cube face:

$$(k/\omega)_1 = (\rho/c_{44})^{1/2} \quad (13B.5.22)$$

The quasishear wave is

$$(k/\omega)_2 = (2\rho)^{1/2} \left\{ c_{11} + c_{44} - \left[(c_{11} - c_{44})^2 \cos^2 2\phi + (c_{12} + c_{14})^2 \sin^2 2\phi \right]^{-1/2} \right\} \quad (13B.5.23(a))$$

The quasilongitudinal wave is

$$(k/\omega)_3 = (2\rho)^{1/2} \left\{ c_{11} + c_{44} + \left[(c_{11} - c_{44})^2 \cos^2 2\phi + (c_{12} + c_{14})^2 \sin^2 2\phi \right]^{-1/2} \right\} \quad (13B.5.23(b))$$

$$\cos \phi = L$$

Propagation in a plane passing through a cube face diagonal is very complex analytically; the effects can be seen more clearly in the graphical plots.

In cubic crystals, there will be special symmetry directions such as $\langle 100 \rangle$, $\langle 110 \rangle$ and $\langle 111 \rangle$ where there will be one pure longitudinal mode and the other two pure shear modes. In other directions, there are one quasilongitudinal and two quasishear modes.

The direction of the energy flux of the pulse is the normal of the corresponding slowness surface at the symmetry directions $\langle 100 \rangle$, $\langle 110 \rangle$, $\langle 111 \rangle$. The energy flux will be parallel to the wave front normal.

Hexagonal materials behave orthotropically in the xy plane normal to the z axis. In this plane, shear and longitudinal modes are pure, that is independent of propagation direction.

A pure shear mode is polarized normal to the z axis:

$$\left(\frac{k}{\omega}\right)_2 = \left(\frac{\rho}{c_{66}}\right)^{1/2} \quad (13B.5.24)$$

A pure shear mode polarized parallel to the z-axis:

$$\left(\frac{k}{\omega}\right)_1 = \left(\frac{\rho}{c_{44}}\right)^{1/2} \quad (13B.5.25)$$

A pure longitudinal mode:

$$\left(\frac{k}{\omega}\right)_3 = \left(\frac{\rho}{c_{11}}\right)^{1/2} \quad (13B.5.26)$$

13B.5.2.1 Graphical Illustrations of Slowness Surfaces

In adjacent isotropic media with different acoustic properties, an incident wave may be reflected or refracted, Figure 13B.5.7(a). In contrast to an electromagnetic wave, acoustic waves can be birefringent or trirefringent; two or three transmitted (refracted) waves may be generated from the incident wave.

Figure 13B.5.8 illustrates electromagnetic and acoustic wave behavior for an incident wave propagating in the yz plane. As noted, the electromagnetic wave velocity (Snell's Law) is independent of propagation and polarization direction so the slowness surface is a single sphere, and

$$\left(\frac{\omega}{v_e}\right) \sin \theta_1 = \left(\frac{\omega}{v_e}\right) \sin \theta_R = \left(\frac{\omega}{v_e}\right) \sin \theta_T \quad (13B.5.27)$$

FIGURE 13B.5.8. Derivation of Snell's Law Relations from the Slowness Surface Isotropic Media

Acoustic media are birefringent even in isotropic media (Figures 13B.5.7(b) and 13B.5.8):

$$\left(\frac{\omega}{V_e}\right) \sin \theta_\ell = \left(\frac{\omega}{V_e}\right) \sin \theta_s = \left(\frac{\omega}{V_\ell}\right) \sin \theta_\ell = \left(\frac{a}{V_s}\right) \sin \theta_s \quad (13B.5.28)$$

Figure 13B.5.9 illustrates the geometric limitations possible with regard to scattering or internal reflection of the incident wave. Such scattering, Figure 13B.5.9(a)), and evanescence occurs at angles of incidence greater than some critical value:

$$\left(\theta_{cr}\right)_e = \sin^{-1} \frac{V_e}{V'_e} \quad (13B.5.29)$$

CRITICAL ANGLE FOR ELECTROMAGNETIC WAVES.

(A)

ELECTROMAGNETIC FIELD DISTRIBUTION ALONG THE y DIRECTION UNDER CONDITIONS OF TOTAL INTERNAL REFLECTION IN THE UPPER MEDIUM.

(B)

CRITICAL ANGLES FOR AN INCIDENT SHEAR WAVE ($V'_s/V_s > 1$).

(C)

SHEAR-WAVE INCIDENCE

$V_s > V'_I > V'_s$	1 CRITICAL ANGLE
$V'_I > V_s > V'_s$	2 CRITICAL ANGLES
$V'_I > V'_s > V_s$	3 CRITICAL ANGLES

LONGITUDINAL-WAVE INCIDENCE

$V_I > V'_I > V'_s$	NO CRITICAL ANGLE
$V'_I > V_I > V'_s$	1 CRITICAL ANGLE
$V'_I > V'_s > V_I$	2 CRITICAL ANGLES

CRITICAL ANGLES FOR ACOUSTIC PLANE WAVE SCATTERING AT A PLANE BOUNDARY BETWEEN ISOTROPIC SOLIDS

(D)

FIGURE 13B.5.9. Examples of Factors Influencing Transmission or Internal Reflection of Acoustic Wave in Isotropic Media

Real values of the critical angle occur only when $V_e < V'_e$.

Figure 13B.5.9(c) illustrates the more complicated situation in acoustic waves where several critical angles may occur. See Figure 13B.5.9(d).

In anisotropic media, the slowness diagrams become much more complicated. Examples are given for single crystals of iron, nickel, stainless steels and of stainless steel weld metal to illustrate similarities among the group.

In anisotropic media, the wave vector amplitude will vary in a complicated fashion with changes in angle of propagation, making an analytic definition of Snell's Law difficult; however, the values are obtainable graphically from the slowness surface. In anisotropic materials, the angle situation is more complex as can be seen in Table 13B.5.1.

TABLE 13B.5.1. Comparison of Refracted Wave Modes

Medium	Electromagnetic Waves(a)	Acoustic Waves
Isotropic	One refracted wave direction	Two wave velocities possible for each propagation direction; two refracted wave directions (birefringence) possible.
Anisotropic	Same as acoustic-isotropic	Three wave velocities possible for each propagation direction; three refracted wave directions may occur (tri-refringence).

(a) Limited to one wave velocity for each propagation direction.

Above anisotropic case is illustrated in Figure 13B.5.10.

Figure 13B.5.11 illustrates some of the unusual features of anisotropic scattering that arise from nonparallelism of the wave vector k and the group velocity V_g . The illustration refers to a cubic crystal with propagation in a cube face, the yz plane, and with a free boundary lying along the xz plane. Since the second medium is vacuum, only reflected waves occur. There is one pure shear wave polarized normal to the cube face, and quasishear and quasi longitudinal waves polarized in the cube face. The pure shear wave has only two stress components, T_{xy} and T_{xz} , while the quasishear and

FIGURE 13B.5.10. Scattering of a Quasilongitudinal Plane Wave at a Boundary Between Anisotropic Media, Showing Construction of the Scattering Angles from the Slowness Surfaces

quasilongitudinal waves have stress components T_{yy} , T_{zz} , and T_{yz} . Consequently, the quasishear and quasilongitudinal waves are not coupled to the pure shear wave by the free surface conditions:

$$T_{yz} = T_{yy} = T_{zz} = 0$$

In Figure 13B.5.11, both the incident and reflected quasishear waves have positive values of k_z , but negative values of $(V_g)_z$. Under these conditions, the ray vectors (or energy flow directions) and the wave fronts (or surface of constant phase) have the relative orientations shown in Figure 13B.5.12.

$$(1) \left(\frac{\rho}{c_{11} + c_{44}(1 - 1/A)} \right)^{1/2}$$

$$(3) (\rho/c_{44})^{1/2}$$

$$(2) (\rho/c_{11})^{1/2}$$

$$(4) (\rho A/c_{44})^{1/2}$$

$$\text{ANISOTROPY FACTOR } A = \frac{2c_{44}}{c_{11} - c_{12}}$$

FIGURE 13B.5.11. Quasishear Scattering at an [010]-Oriented Free Boundary of a Cubic Crystal. Incidence is in the (100) plane.

FIGURE 13B.5.12. Ray Vectors (Solid Arrows) and Wave Fronts (Dashed Lines)

Anisotropy of the acoustic slowness surface can lead to unusual wave scattering effects at a plane boundary between two media. An example is when the boundary is normal to a three-fold crystal symmetry axis in the second medium, and the incident wave impinges normally on the boundary. The cube diagonal direction of a cubic crystal $[111]$ is one example (Figure 13B.5.13).

Waves are either pure transverse or pure longitudinal, and the transverse (or shear) waves are degenerate. Also, the shear slowness curves cross the symmetry axis at an angle. Figure 13B.5.13(b) illustrates this behavior for the $[111]$ direction in a cubic crystal, where one of the shear curves corresponds to a $[1\bar{1}0]$ -polarized pure shear wave and the other reduces to a pure shear wave polarized at right angles to the plane containing $[1\bar{1}0]$ and $[111]$.

The energy (or group) velocity is always normal to the slowness surface. This means that the energy velocities of the shear waves propagating along $[111]$ in Figure 13B.5.13(b) are both deflected away from k . The group velocity of the $[1\bar{1}0]$ -polarized shear wave is deflected toward the $[001]$ axis and the group velocity of the other shear wave is deflected toward the $[110]$ axis. There are, however, still further complications. Figure 13B.5.13(b) represents slowness curves for waves propagating in the plane defined by the $[110]$ and $[001]$ crystal directions, but it is clear from the cubic symmetry of the crystal that identical curves will be obtained for propagation in any plane that passes through a cube edge and a cube face diagonal. Slowness curves for the plane passing through $[100]$ and $[011]$, therefore, appear as shown in Figure 13B.5.13(c). The shear waves propagating along $[111]$ now have different polarizations than those given in Figure 13B.5.13(b). The unusual feature is that the group velocity directions are also different than they were in Figure 13B.5.13(b). For the $[0\bar{1}\bar{1}]$ -polarized pure shear wave in Figure 13B.5.13(c), the group velocity is deflected toward $[100]$ and the other shear wave is deflected toward $[011]$. Slowness curves for the plane passing through $[010]$ and $[101]$ also have the same shapes giving two additional polarization and energy velocity directions for the pure shear waves traveling along $[111]$. This kind of behavior always occurs when shear waves propagate along a three-fold crystal symmetry axis.

FIGURE 13B.5.13. Ray Vectors (Solid Arrows) and Wave Fronts (Dashed Lines)

FIGURE 13B.5.13. Group Velocity Deflections for Pure Shear Waves Travelling Along the Threefold Symmetry Axis [111] in a Cubic Crystal

An example of the variation in slowness surface of single crystal austenitic stainless steel at various angles, β , is given in Figure 13B.5.14.

FIGURE 13B.5.14. Cross Sections of the Slowness Surfaces Corresponding to $\beta = 0^\circ, 15^\circ, 30^\circ, 45^\circ$. The small arrows in (a) and (d) represent the directions of energy flux.

Hexagonal crystals represent a special case because of orthotropic symmetry about the z axis. Because of the solidification pattern of austenitic weldments, there is a very strong preferential axial solidification and a rather random orientation about this axis. The pattern is similar to that observed in hexagonal metals. The velocity, wave, and slowness surfaces in the

xy plane are quite simple. Figure 13B.5.15 illustrates the slowness surface. The velocity surface will invert the circles so that the longitudinal wave is the largest. Since the waves are all pure, not quasi, the wave surface will duplicate the velocity surface.

The surfaces containing the z axes are quite complicated and will not be discussed here.

FIGURE 13B.5.15. Hexagonal Crystal Classes. Propagation in XY plane (normal to the Z-axis) orthotropic symmetry slowness surface.

xy plane are quite simple. Figure 13B.5.15 illustrates the slowness surface. The velocity surface will invert the circles so that the longitudinal wave is the largest. Since the waves are all pure, not quasi, the wave surface will duplicate the velocity surface. The surfaces containing the z axes are quite complicated and will not be discussed here.

FIGURE 13B.5.15. Hexagonal Crystal Classes. Propagation in XY plane (normal to the Z-axis) orthotropic symmetry slowness surface.

The angular deviation of the energy (group) velocity vector from the phase velocity vector is sensitive to the physical properties of a solid, such as the density and elastic constant. Such beam deviation or skewing may result in internal reflections or failure to detect because the beam falls outside the transducer region. In general, there is a tendency for the beams to preferentially travel along directions associated with higher phase velocities. Therefore, quasilongitudinal waves will deviate toward $\langle 110 \rangle$ and $\langle 111 \rangle$, and quasi shear waves will deviate towards $\langle 100 \rangle$.

In the generation of velocity, slowness, and wave surfaces of single crystals or oriented structures, the following values are required, using a cubic crystal as an example:

- An angle θ preferably measured from some direction such as $\langle 001 \rangle$; however, an arbitrary position can be used.
- Elastic constants of the crystal c_{11} , c_{12} , c_{44} .
- Density of the crystal.

Measured values related to θ are

- longitudinal and shear velocities (V_L , V_{S_V} , V_{S_H})
- angles ϵ_L , ϵ_{S_V} , ϵ_{S_H} (use Figure 13B.4.4)
- ratios $c_{44}/\rho V_L$, $c_{44}/\rho V_{S_V}$, $c_{44}/\rho V_{S_H}$
- ratios $\rho V_L^2 - c_{44}/\rho V_L \cos \epsilon_L$; $\rho V_{S_H}^2 - c_{44}/\rho V_{S_H} \cos \epsilon_{S_H}$;

$$\rho V_{S_V}^2 - c_{44}/\rho V_{S_V} \cos \epsilon_{S_V}$$

It is possible to construct all three surfaces using the above values.

With respect to skewing, the angle Δ (Figure 13B.4.4) is required as a function of θ for L, S_H , S_V . This can be obtained using the information derived from Figure 13B.4.4:

Desired value: angle Δ_L (Using L Wave as an Example)

Known values: $ON = V_L$

$$OI = c_{44}/\rho V_L$$

$$IN = V_L - c_{44}/\rho V_L$$

$$IP = \frac{IN}{\cos \epsilon} = \frac{V_L - c_{44}/\rho V_L}{\cos \epsilon_L}$$

To determine $\sin \epsilon$ using:

$$\epsilon = \frac{c_{44}/\rho V_L - V_L}{\cos \epsilon_L}$$

$$\sin \epsilon = \frac{NP}{IP}$$

$$NP = IP \sin \epsilon_L = \frac{\rho V_L^2 - c_{44}}{\rho V_L \cos \epsilon_L} \sin \epsilon_L$$

$$\tan \Delta_L = \frac{NP}{ON} = \frac{\rho V_L^2 - c_{44}}{\rho V_L \cos \epsilon_L} \sin \epsilon_L \cdot \frac{1}{V_L}$$

Values $\frac{\rho V_L^2 - c_{44}}{\rho V_L \cos \epsilon}$; $\frac{1}{V_L}$; $\sin \epsilon$ are known.

$$\tan \Delta_S = \frac{NP}{ON} = \frac{c_{44} - \rho V_S^2}{\rho V_S \cos \epsilon_S} \sin \epsilon_S \frac{1}{V_S}$$

Therefore, $\tan \Delta$ can be calculated yielding Δ .

As noted previously, the magnitude of the skewing angle depends on the elastic constants. For example, the following are the values in $\langle 100 \rangle$ and $\langle 110 \rangle$ directions:

$\langle 100 \rangle$	$\langle 110 \rangle$
$\rho V_L^2 = c_{11}$	$\rho V_L^2 = \frac{1}{2} (c_{11} + c_{12} + 2c_{44})$
$\rho V_{S_H}^2 = \rho V_{S_V}^2 = c_{44}$	$\rho V_{S_H}^2 = c_{44}$
	$\rho V_{S_V}^2 = \frac{1}{2} (c_{11} - c_{12})$

Therefore, as the magnitude of elastic constants increases, velocities increase for a given density; as the magnitude of density increases, velocity decreases. For metals of interest, the density will be about 9 g/cm^3 .

The relative magnitudes of the vectors IN and IP establish ϵ . This, in turn, sets the magnitude of Δ . Since

$$IN = V_L - c_{44}/\rho V_L \quad \text{or} \quad c_{44}/\rho V_S - V_S$$

$$IP = \frac{V_L - c_{44}/\rho V_L}{\cos \epsilon_L} \quad \text{or} \quad \frac{c_{44}/\rho V_S - V_S}{\cos \epsilon_S}$$

Examination of Figure 13B.4.4 confirms that incremental increases of c_{44} for a given density results in an increase of Δ with respect to ϵ . This is particularly apparent in the shear-wave case.

Some examples of beam skewing for the (001) plane and for the (110) plane are given in Figures 13B.6.1 and 13B.6.2. The values confirm the preferred use of longitudinal waves near 45° for examination of austenitic weldments.

FIGURE 13B.6.1. Variation in the Angle of Deviation Δ in the (100) Planes for (1) Single-Crystal Stainless Steel; (2) Iron; (3) 316 SS Weld Metal; (4) Nickel (Analytically)

FIGURE 13B.6.2. Variation in the Angle of Deviation in the (110) Plane for (1) Single-Crystal Stainless Steel; (2) Dendritic Structures of Stainless Steel; (3) Analytic Only for Nickel (Presume Single-Crystal)

FIGURE 138.6.2. Variation in the Angle of Deviation in the (110) Plane for (1) Single-Crystal Stainless Steel; (2) Dendritic Structures of Stainless Steel; (3) Analytic Only for Nickel Pressure Single Crystal.

In systems of relatively high symmetry, such as cubics or hexagonals, it is possible to infer the geometry of surfaces from comparable crystal structures in the absence of knowledge of the elastic constants. With a series of parallel surfaces such as in an octahedron, one can establish directions of maximum beam velocity and back calculate the elastic constants. This procedure was used by Juva et al.^(13B.7.1), Silk et al.^(13B.7.2), and Kupperman and Reimann^(13B.7.3) with single crystals and weld crystals of austenitic stainless steel. Values are given in Table 13B.7.1.

TABLE 13B.7.1. Values of Elastic Constants for Austenitic Stainless Steels (N/m²)

Source	c_{11}	c_{12}	c_{44}
Juva et al. ^(13B.7.1)	2.036×10^{11}	1.335×10^{11}	1.298×10^{11}
Silk et al. ^(13B.7.2)	2.230×10^{11}	1.318×10^{11}	1.099×10^{11}
Kupperman & Reimann ^(13B.7.3)	$2.05 \pm 0.02 \times 10^{11}$	$1.33 \pm 0.02 \times 10^{11}$	$1.25 \pm 0.005 \times 10^{11}$

The following questions were posed prior to writing this chapter:

1. How are the various surfaces calculated?
2. What is the significance of the various surfaces with respect to UT beam behavior and structure?
3. What can be inferred from the various surfaces with regard to beam skewing, beam convergence, divergence, optimum examination angle and optimum structure for examination?

Hopefully, answers to these questions have been developed.

It is apparent that the slowness surface is the most powerful from an interpretive point of view. Once the slowness surface is known, the others can be developed.

In systems of relatively high symmetry, such as cubic or hexagonal, it is possible to infer the geometry of surfaces from comparable crystal structures in the absence of knowledge of the elastic constants. With a series of parallel surfaces such as in an octahedron, one can establish directions of maximum beam velocity and back calculate the elastic constants. This procedure was used by Jura et al. (138.V.1), Stik et al. (138.V.2), and Kuperman and Reimann (138.V.3) with single crystals and weld crystals of austenitic stainless steel. Values are given in Table 138.V.1.

TABLE 138.V.1. Values of Elastic Constants for Austenitic Stainless Steels (N/m²)

Source	C_{11}	C_{12}	C_{44}
Jura et al. (138.V.1)	2.036×10^{11}	1.335×10^{11}	1.298×10^{11}
Stik et al. (138.V.2)	2.230×10^{11}	1.318×10^{11}	1.099×10^{11}
Kuperman & Reimann (138.V.3)	$2.05 \pm 0.05 \times 10^{11}$	$1.33 \pm 0.05 \times 10^{11}$	$1.25 \pm 0.005 \times 10^{11}$

The following questions were posed prior to writing this chapter:

1. How are the various surfaces calculated?
2. What is the significance of the various surfaces with respect to UT beam behavior and structure?
3. What can be inferred from the various surfaces with regard to beam skewing, beam convergence, divergence, optimum examination angle and optimum structure for examination?

Hopefully, answers to these questions have been developed.

It is apparent that the slowest surface is the most powerful from an interpretive point of view. Once the slowest surface is known, the others can be developed.

- 13B.1.1 Musgrave, M. J. P., "On the Propagation of Elastic Waves in Aelotropic Media: I General Principles". Proc. R. Soc. London A 226:339-355, 1954.
- 13B.1.2 Miller, G. F. and Musgrave, M. J. P., "On the Propagation of Elastic Waves in Aelotropic Media: III Media of Cubic Symmetry". Proc. R. Soc. London A 236:352-383, 1956.
- 13B.1.3 Auld, B. A., Acoustic Fields and Waves in Solids. Vol. I, Wiley-Interscience, New York, New York, 1973.
- 13B.1.4 Auld, B. A., Acoustic Fields and Waves in Solids. Vol. II, Wiley-Interscience, New York, New York, 1973.
- 13B.7.1 Juva, A. and Lenkkeri, L., "The Effects of Anisotropy on the Propagation of Ultrasonic Waves in Austenitic Stainless Steel". CSNI Specialist Meeting on The Applicability of Ultrasonic Testing Techniques for Coarse Grain Austenitic Welds. Committee on the Safety of Nuclear Installations of OECD Nuclear Energy Agency, Brussels, Belgium, May 29-30, 1980.
- 13B.7.2 Silk, M. G., Lidington, B. H. and Hammond, G. F., "A Time Domain Approach to Crack Location and Sizing in Austenitic Welds." Br. J. Nondestr. Test. 22(2):55-61, March 1980.
- 13B.7.3 Kupperman, D. S. and Reimann, K. F., "Ultrasonic Wave Propagation and Anisotropy in Austenitic Stainless Steel Weld Metal." IEEE Trans. Sonics Ultrason. SU-27(1):7-15, January 1980.

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- 138.1.3 Auld, B. A., Acoustic Fields and Waves in Solids, Vol. I, Wiley-Interscience, New York, New York, 1973.
- 138.1.4 Auld, B. A., Acoustic Fields and Waves in Solids, Vol. II, Wiley-Interscience, New York, New York, 1973.
- 138.7.1 Gysa, A. and Lankert, L., "The Effects of Anisotropy on the Propagation of Ultrasonic Waves in Austenitic Stainless Steel", ESN Symposium Meeting on The Applicability of Ultrasonic Testing Techniques for Coarse Grain Austenitic Welds, Committee on the Safety of Nuclear Installations of OECD Nuclear Energy Agency, Brussels, Belgium, May 29-30, 1980.
- 138.7.2 Stik, M. G., Lidington, B. H. and Hammond, G. F., "A Time Domain Approach to Crack Location and Sizing in Austenitic Welds", Br. J. Nondestruct. Test. 22(2):55-61, March 1980.
- 138.7.3 Kuperman, D. S. and Reimann, K. F., "Ultrasonic Wave Propagation and Anisotropy in Austenitic Stainless Steel Weld Metal", IEEE Trans. Sonics Ultrason. SU-27(1):2-12, January 1980.

CHAPTER 14

THE ASME CODES: TESTING TECHNIQUES, ANALYTIC PROCEDURES AND SUGGESTED MODIFICATIONS

CHAPTER 14

THE ASME CODES: TESTING TECHNIQUES, ANALYTIC PROCEDURES
AND SUGGESTED MODIFICATIONS

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CHAPTER 14

THE ASME CODES: TESTING TECHNIQUES, ANALYTIC PROCEDURES AND SUGGESTED MODIFICATIONS

14.1

INTRODUCTION

We will examine the ASME (V and XI) Code requirements relevant to the ultrasonic examination of reactor systems and components. A review of the mandatory appendices for ultrasonic examinations should permit an assessment of their strengths and weaknesses. Later sections of the chapter will review the reliability values obtained in the examination of piping and pressure vessels either as components or as simulated components (e.g., test blocks). These values highlight some of the limitations inherent in the code-approved ultrasonic procedures. Finally, suggestions will be made for evolutionary (stop-gap) changes versus revolutionary changes. Some could be made through ASME Code channels; others could complement the Code, through Regulatory Guides or similar approaches.

THE ASME CODES: TESTING TECHNIQUES, ANALYTIC PROCEDURES AND SUGGESTED MODIFICATIONS

INTRODUCTION

14.1

We will examine the ASME (V and XI) Code requirements relevant to the ultrasonic examination of reactor systems and components. A review of the many existing approaches for ultrasonic examinations should permit an assessment of their strengths and weaknesses. Later sections of the chapter will review the reliability values obtained in the examination of piping and pressure vessels either as components or as simulated components (e.g., test blocks). These values highlight some of the limitations inherent in the code-approved ultrasonic procedures. Finally, suggestions will be made for evolutionary (step-by-step) changes versus revolutionary changes. Some could be made through ASME Code changes; others could complement the Code, through regulatory guides or similar approaches.

- What is the purpose of inspection, particularly ultrasonic?

The purpose is to locate flaws in the pressure retaining boundary. Flaws of significant size must be located, and their size and shape determined. The probability of finding the flaw should be very high; the accuracy of defining the flaw is not so critical but must be conservative.

It is widely recognized that radiographic and ultrasonic examinations are complementary tools, and that one does not take the place of the other in locating all flaws.

Although both techniques are often capable of detecting the same internal flaw, it has been repeatedly demonstrated that certain flaws will go undetected by radiography, but be readily found ultrasonically. Conversely, though less often, radiography has been demonstrated to find flaws which were undetected by ultrasonics.

Two examples are presented.

First, consider a smooth-surfaced, straight-sided flaw, such as lack-of-fusion, perpendicular to the testing surface. Detectability by radiography will be very good. Detectability by (conventional) single search unit UT will be poor for a smooth-sided flaw; a tandem search unit technique will be effective unless the flaw is so tight that ultrasound transmits readily through it.

Second, consider an irregular crack-like flaw oriented perpendicular to the testing surface. Detectability by radiography will be poor unless the voids oriented parallel to the beam are significant. Conventional UT will detect it easily because of reflections from the many facets of the crack perpendicular to the ultrasonic beam. A tandem search unit will give inferior results. Thus, RT and UT are complementary.

Section III of the ASME code relies on radiography for volumetric examination, while Section XI, because of radiation and access difficulties, must rely on ultrasonics. Responsibility for the examination also shifts, from the Manufacturer for Section III to the Owner for Section XI. This leaves the way open for conflict.

- Why did Section XI issue its own procedures?

The ultrasonic procedures available in the Code, such as Article 5 of Section V, while adequate for flaw detection, did not provide for measurement of flaw size to relate to the Section XI acceptance standards; they were dependent on the experience and judgment of the individual test operator to determine the nature and size of reflectors.

- What are we talking about?

Appendix I of ASME XI and Article 4 of Section V, which replaced Appendix I in winter 1976, contains mandatory requirements for the conduct of ultrasonic examination of welds in Class 1 and Class 2 ferritic vessels. The technique not only detects and locates flaw indications within the weld and adjacent base metal, but also provides length and through-wall dimensions of the indications.

Appendix III contains mandatory requirements for the conduct of ultrasonic examination of welds in Class 1 and Class 2 ferritic piping systems. Flaw indication sizing criteria are included.

It is expected that Appendix III will be replaced with text from Section V. The appendices and the articles are applied in accordance with IWA-2200 when the methods are specified by Tables IWB-2500 and IWC-2500.

IWA-2232 covers application of ultrasonic examination. In Summer 1978,

- (a) makes Article 4 (Section V) mandatory for Class 1 and 2 ferritic vessels greater than 2 in. thick.
- (b) provides for use of Appendix III for ferritic piping, with the modification that 50% of reference level be recorded.
- (c) provides that everything not falling within the (a) and (b) categories shall be examined by Article 5.

While recording at 50% reference level, everything is evaluated and sized at (100%) of reference level.

Also note that other ultrasonic techniques may be employed if qualified under IWA-2240.

After flaw indications are detected and recorded, they must be evaluated (IWA-3000). The evaluations are divided into two categories:

- (1) Indications not exceeding the acceptable sizes cited in IWB-3500.
- (2) Indications exceeding the acceptable sizes of IWB-3500. (They may be evaluated in accordance with Appendix A of ASME XI. Results of Appendix A may be
 - a) size acceptable; continue to monitor
 - b) size unacceptable; repair.)

Typical examples of standards are given in Figure 14.2.1 (IWB-3330) and Tables 14.2.1 and 14.2.2 (IWB-3510.1 and IWB-3510.2).

Table 14.2.3, culled from the various ASME XI editions and addenda, provides an overview of critical actions influencing NDE.

14.2.1 Objectives of UT Appendices

The primary objectives of vessel weld examinations may be summarized briefly:

1. search the entire weld volume
2. locate reflectors accurately
3. define reflector size
4. achieve reproducibility between successive examinations over the life of the unit.

Complications are added to examination of piping welds:

1. It is essential that reflections from geometric conditions associated with the weld be differentiated from flaws.
2. It is essential that dependence on the judgment of the individual operator be reduced.

FIGURE 14.2.1 (IWB-3330). Multiple Planar Flaws Oriented in Plane Normal to Pressure Retaining Surface

The use of Appendices I and III has improved flaw detection; however, continued improvement will be necessary. For example, examinations 1) through overlay cladding on vessels, 2) of austenitic piping, 3) of tri-metallic weld

TABLE 14.2.1 (IWB-3510.1). Allowable Planar Indications

Material Ferritic Steels that Meet the Requirements of
NB-2331 and have specified minimum yield strength of
50 ksi or less at 100 F
Thickness Range: 4 in. and Greater

Aspect Ratio, a/l (1)	Surface Indications $a/t, \%$ (2)	Subsurface Indications $a/t, \%$ (2,3)
0	1.8	2.3
0.05	2.0	2.4
0.10	2.2	2.6
0.15	2.4	2.9
0.20	2.7	3.2
0.25	3.1	3.7
0.30	3.5	4.1
0.35	3.5	4.6
0.40	3.5	5.2
0.45	3.5	5.8
0.50	3.5	6.5

NOTES:

- (1) For intermediate Flaw-aspect ratios, A/l linear interpolation is permissible.
- (2) Component Thickness, t , is measured normal to pressure retaining surface of component. Where the section thickness varies, the average thickness over the length of the indication is the component thickness.
- (3) The total allowable depth of subsurface indication is $2a$.

joints, and 4) of some nozzle geometries are difficult and may not be sufficiently reliable. Additional research and development is required to provide bases for future modifications of the mandatory appendices.

The following are the critical factors in Appendices I, III, and IV:

- use of calibrated (and certified if possible) equipment
- use of trained personnel
- use of a well-defined written procedure

TABLE 14.2.2 (IWB-3510.2). Allowable Laminar Indications

Component thickness, t, in. ¹	Laminar Area A, Sq in. ²
4	18
6	18
8	24
10	30
12 and greater	36

NOTES:

- (1) For intermediate thickness, linear interpolation of area is permissible
- (2) The area of a Laminar Flaw is defined in IWB-3360

- use of calibration blocks that closely duplicate such factors as thickness, acoustic properties, surface finish, etc., of the components being inspected
- daily use of a rigorous, comprehensive calibration procedure that must be completely documented
- use of an examination procedure that closely parallels the calibration procedure so new parameters are not introduced
- extensive documentation of every stage of the examination with attention to a well-defined reference system for locating precisely where the transducer is with respect to a weld.

Items Common to All Appendices

In all cases personnel performing NDE must be qualified in accordance with SNT-TC-1A-1975 (or 1980). Where NDE methods do not exist, it is the responsibility of the owner or his agent to provide some (IWA-2300). The following are required:

Article 5200 (h). - requires the operator's name, certification level and company affiliation.

TABLE 14.2.3. Significant Changes in NDE Requirements in ASME XI Editions and Addenda

Edition/ Addenda	Paragraph	Items/Comments
<u>1970</u>	IS-210	Examination Techniques
	IS-212.1	MT cites ASME III App. IX - 3500
	IS-212.2	PT cites ASME III App. IX - 3600
	IS-213.1	RT cites ASME III App. IX - 3300
	IS-213.2	UT cites ASME III App. IX - 3400
		ASTM-E-114
		ASTM A-435
	IS-214	Permits alternate exams
	IS-311	Evaluation per ASME III on ANSI 31.7, SNT-TC-1A for personnel. (Above emphasized the primary pressure boundary; examination of some Class 2 systems (ECCS, etc.) is inferred. Note that App. IX-3400 requires 20% DAC.)
W-70	IS-120	Deleted comments on Class 2 systems (ECCS, Auxiliary)
<u>1971</u>		No major changes from 1970
	IS-311	Dropped reference to ANSI 31.7
S-71	IS 213.2	Permits 100% DAC to size (wording ambiguous - does App. IX require 20% DAC control for detection?)
W-71	IS-311	Trivial changes regarding sizing criteria
S-72		Nothing significant
W-72		Added Class-2 Systems
S-73	IS-213	Major changes - added App. I (pressure vessel) (emphasis is on calibration)
		Version covered thickness >1-in.
	IS-311	Added evaluation of NDE results
	IS-313	Added flaw characterization
	IS-314	Added flaw standards
	IS-315	Added evaluation standards

TABLE 14.2.3. (contd)

<u>Edition/ Addenda</u>	<u>Paragraph</u>	<u>Items/Comments</u>
	IS-316	Added evaluation analyses per App. A.
W-73		Nothing
<u>1974</u>		New format - IW instead of IS
	IWA-2121	MT cites ASME V Article 7
	IWA-2122	PT cites ASME V Article 6
	IWA-2234	RT cites ASME V Article 2
	IWA-2231	UT cites ASME V Article 5
	IWA-2232	Requires UT in accordance with either App. I or ASME V Article 5 where anything less than 2.5 in. falls under article 5 so 20% DAC must be used
S-74	App. I	Revised scanning requirements and calibration block material
	IWB-2500	Expands base metal subject to UT
W-74		Nothing significant
S-75		Nothing significant; App. I errata
W-75*		Major change in examination philosophy
	IWA-2410	Introduced two examination programs (A & B); required qualification by examination of Level III personnel to SNT-TC-1A-1975. Incorporated App. III (UT Piping)
	IWA-2420	
S-76*		Incorporated (formally) App. IV (ET-S. G. Tubing)
	IWA-2232	Referenced App. I, III and Article 5 but raised 20% DAC to 100% DAC
W-76*		Deleted App. I; replaced with Article 4 of ASME V. (Article 4 parallels App. I in calibration area.)
<u>1977</u>		Minimal changes from final 1974 addenda
S-77	IWA-2300	Expanded criteria for visual testing
W-77	IWA-2300	More visual criteria V-1 through V-4

* 10 CFR 50.55(b) does not accept W-75, S-76, and W-76 Addenda.

TABLE 14.2.3. (contd)

Edition/ Addenda	Paragraph	Items/Comments
S-78	IWA-2232	Spelled out Article 4 and App. III; new requirements to record 50% DAC and size at 100% DAC
	App. IV	Requirement to report tube denting
S-79	IWA-2232	Specific requirements, visual acuity
	III-3320	System calibration at least once a week
	T-523	Definition of written procedure
	Article 5	Expanded calibration block requirements
	Article 5	Overall change in format
W-79	IWA-2232	Specifics regarding use of nozzle dropout, component prolongations, or material with same specification, product form and heat treatment for calibration blocks
	T-432.1.2	Dropped requirement to recalibrate mechanized equipment with change in personnel
	Table	
	T-434.1	Defined perpendicular tolerance on notch reflecting surface
<u>1980</u>		Nothing significant
	S-80	Nothing significant
	W-80,	
	T-431	Must calibrate at beginning and end of vessel weld examination
	W-80	
	IWA-2232	Specifies use of written procedure
	III-3230	Expanded comments on restriction in weld exam
	III-3330	System check change from 4 to 12 hours
	App. IV	Cites Article 8 of ASME V for eddy current; residual App. IV ties to Article 8

Article 7000 -- requires identification of operator(s) and data evaluator(s) who conducted each examination or part thereof.

NDE by UT is in accord with Appendices I or III plus relevant articles (4, 5) of ASME Section V (IWA-2232).

The preservice examination encompasses essentially 100% of all pressure-retaining welds together with a substantial portion of adjacent base metal (IWB-2100).

14.2.2 Ultrasonic Examination

The various factors cited under Procedure Requirements will be discussed in detail, primarily in the context of the 1974 edition of ASME XI.

Where possible the similarities of Appendices I and III will be cited. Obvious differences will be emphasized.

Scope (I-1100, T-410, III-1100)

Defines the ultrasonic (UT) examination method, equipment and reporting requirements for welds. The methods described are used to detect and locate imperfections within the weld and adjacent base material.

Limitations of Scope

I-1200, T-441. Methods are limited to Class 1 and 2 ferritic vessels 2-1/2 in. (2 for Article 4) and over in wall thickness. Clad vessels are included.

III-1100. Limited to Class 1 and 2 piping systems made from ferritic steel in full penetration butt welds and adjacent base metal in piping systems having a nominal wall thickness of 0.20 to 6 in. Examination of base metal shall be applied to each accessible side of the weld for a distance of 1/2 T or 1 in., whichever is smaller.

III - Supplement 7 - Austenitic Welds. The UT examination of welds in high alloy steels, or high nickel alloys, and of dissimilar metal welds between combinations of these materials and carbon or low alloy steels may, because of the inherent coarse-grained structure of the weld, be subject to marked variations in attenuation, velocity, reflection, and refraction at grain boundaries. If the rules of Appendix III are used, the following recommended modifications,

such as "other angles may be used where metallurgical characteristics impede effective use of 45° angle beam (III-4410)," should be used.

Procedure Requirements (I-1300, T-422, III-2300)

Ultrasonic examinations are performed in accordance with a written procedure. Each procedure includes at least the following information:

- (a) weld types and configurations to be examined including thickness dimensions (materials and product form—casting, forging, plate, etc.)
- (b) the surface or surfaces from which the examination shall be performed
- (c) surface condition
- (d) couplant
- (e) technique (straight beam, angled beam) contact or immersion
- (f) angles and mode of wave propagation in the material
- (g) (search unit) type, frequency and transducer size(s)
- (h) special search units, wedges, shoes, or saddles, if used, and type and length of search unit cable
- (i) ultrasonic instrument (types)
- (j) description of calibration
- (k) directions (and extent) of scanning
- (l) (data to be recorded and) method of recording (manual or automatic)
- (m) automatic alarm and recording equipment or both, if used
- (n) rotating, revolving, or scanning mechanisms, if used

In III and Article 4 the following are included:

- (o) personnel qualification requirements
- (p) approval of the procedure as required by IWA-2120 (in heading) of I.

Calibration Blocks (T-434, III-3400)

Vessel calibration blocks must duplicate as closely as possible the material of the vessel being examined. The initial position in which they must be

placed in relation to the vessel has been relaxed somewhat. Data have been presented to the ASME XI committee providing the bases for reasonable relaxations to permit material of the same material specification and heat treatment as included in the vessel. The calibration block is significantly larger than the standard Section V block to reduce the effect of sidewall reflections, and provide full beam path conditions. It also contains six mandatory calibration holes and two notches, and is clad (if the component is clad) to provide the most complete foreseeable basis for duplication of the actual workpiece. The calibration block must be retained on the vessel site, so that it will always be available for future examinations.

Code Case N-278 provides a design for a single vessel calibration block that can be used for a range of material thicknesses (Figure 14.2.2). The requirements for calibration blocks for piping have been made even more exacting, in a sense, than those for vessels; the Appendix III blocks must be of the same material, nominal diameter, and wall thickness as the piping being examined. However, Code Case N-98 permits ± 0.25 in. tolerance on calibration blocks cited in Article 5 when thickness is less than 1 in.

Calibration sensitivity is based on the reflections from I.D. and O.D. surface notches, unlike the side-drilled holes used to determine sensitivity of vessel weld examinations. Surface notches have been used to be consistent with the concern for surface crack detection. A "calibrated" surface crack would be the ideal calibration reflector; a surface notch is the best approximation. Side-drilled holes may be used to determine shape of a distance-amplitude correction (DAC) curve.

Representative calibration blocks for vessels and piping are shown in Figures 14.2.3 (I-3131) and 14.2.4a and b [III-3430(a), III-3430(b)], as well as Figure 14.2.5 (I-3240) for curved calibration blocks for pressure vessels.

- Why does Appendix I (Article 4) have such rigorous calibration requirements?

The round-robin ultrasonic examinations conducted by the Pressure Vessel Research Committee (PVRC) on heavy-section weld specimens 201 and 251J revealed

FIGURE 14.2.2. Alternative Basic Calibration Block (Permitted Under Code Case N-278)

NOTES:

1. Holes drilled and reamed to 5/16 in. dia. positioned at 1 in. intervals through the calibration block thickness. The five side-drilled holes located below center thickness are located near side, the five upper holes are located on the far side.
2. Holes installed as in note 1 but positioned on a scribe line shown at 1 in. intervals through the thickness. The holes shall be alternated side to side, as shown so that the distance between any two holes is 2 in. (top and bottom holes are 1 in. from the surface).
3. Notches (2) top and bottom as shown, 2 in. long to 1/4 in. dia. flat end mill notches 2% T deep through clad notch is 2% deep into the base metal.
4. The tolerances for hole diameters shall be $\pm 1/32$ in. notch depth tolerance shall be +10 and -20%. The tolerance on hole location through the thickness shall be $\pm 1/8$ in.
5. For basic calibration blocks 4 in. and less the side-drilled holes shall be at T/4 in. intervals through the thickness and hole diameter shall be 3/16 in.
6. Calibration or DAC curves obtained using the block shall include all side-drilled holes representing the weld thickness to be examined.
7. Use of surface notch and surface notch response calibration is optional.

* Minimum dimensions

** Basic calibration block thickness shall equal or exceed the maximum weld thickness to be examined.

WELD THICKNESS (t)	BASIC CALIBRATION BLOCK THICKNESS (T)	HOLE DIAMETER
OVER 2 in. THRU 4 in.	3 in. OR t	3/16 in.
OVER 4 in. THRU 6 in.	5 in. OR t	1/4 in.
OVER 6 in. THRU 8 in.	7 in. OR t	5/16 in.
OVER 8 in. THRU 10 in.	9 in. OR t	3/8 in.
OVER 10 in.	*	*

* FOR EACH INCREASE IN THICKNESS OF 2 in. OR FRACTION THEREOF THE HOLE DIAMETER SHALL INCREASE 1/16 in.

FIGURE 14.2.3 (I-3131). Basic Calibration Block

that there was a great variability in procedures and results among the examiners (see Welding Research Council Bulletins 221, 252). A revised PVRC test procedure (6/27/74), which was the basis for Appendix I, was developed to reduce variability and to enhance reliability of detection and location of flaws. It was verified by use on round-robin ultrasonic examinations on PVRC specimens 155, 202, and 203 (see WRC Bulletin 257).

Calibration for Examination, Appendix I (Article 4)

The general method of calibration is semi-mandatory. The UT operator may choose his own method so long as he achieves the following:

- sweep range calibration over the examination range

FIGURE 14.2.4a (III-3430(a)). Allowable Notch Configurations

FIGURE 14.2.4b (III-3430(b)). Recommended Design for Basic Calibration Block.

- sensitivity calibration of the examination system
- distance amplitude correction (DAC)
- position calibration with respect to the front of the search unit and the examination surface
- correction for planar reflectors perpendicular to the examination surface at or near the surface
- beam angle and beam spread measurement.

FIGURE 14.2.5 (I-3240). Curved Basic Calibration Block (Appendix I, ASME XI)

Calibration sensitivity is based upon the ultrasonic reflections from side-drilled holes in the calibration block.

In all calibrations, it is important that maximum indications be obtained with the sound beam oriented perpendicular to the axis of the calibration reflector. The center line of the search unit shall be at least $3/4$ in. from the nearest side of the block or pipe. (Rotation of the beam into a corner formed by the reflector and the side of the block may produce a higher amplitude signal at a longer beam path: this beam path shall not be used for calibration.)

For contact examination, the temperature of the examination and basic calibration block surfaces shall be within 25°F . For immersion examination, the couplant temperature for calibration shall be within 25° of that for scanning, or appropriate compensations for angle changes shall be made.

Note that calibration includes the complete ultrasonic examination system. Any change in search units, shoes, couplants, cables, ultrasonic instruments,

recording devices, or any other parts of the examination system is cause for a calibration check. The original calibration must be performed on the basic calibration block. Calibration checks may be performed on a basic calibration block simulator or calibration reference block, but must include a check of the entire examination system.

Each calibration shall be performed from the surface (clad or unclad) corresponding to the surface of the component from which the examination will be performed.

Linearity

Two very important operations in instrument calibration concern amplitude and amplitude control linearity. The specific operations required in such calibrations follow:

Supplement 5 - Screen Height Linearity. To verify the ability of the ultrasonic instrument to meet the linearity requirement, position an angle beam search unit as shown in Figure 14.2.6 (III-S5-1) so that echoes can be observed from any two reflectors in a calibration block. Adjust the search unit position to give a 2 to 1 ratio of amplitudes between the two echoes, with the larger set at 80% of full screen height. Without moving the search unit, adjust sensitivity (gain) to successively set the larger echo from 100% to 20% of full screen height, in 10% increments (or 2-dB steps if a fine control is not available), and read the amplitude of the smaller echo at each setting. The reading must be 50% of the larger amplitude, within 5% of full screen height. The settings and readings must be estimated to the nearest 1% of full screen. Alternatively, a straight beam search unit may be used on any calibration block that will provide the signal differences.

Supplement 6 - Amplitude Control Linearity. To verify the accuracy of the amplitude control in the ultrasonic instrument, position a search unit so that an echo from one reflector in a calibration block is peaked on the screen. With the increases and decreases in attenuation shown in Table 14.2.4 the echo amplitude must fall within the specified limits. Convenient reflectors from any calibration block may be used with angle or straight beam search units.

FIGURE 14.2.6 (III-S5-1). Linearity

TABLE 14.2.4. Range of the Amplitude Control in Ultrasonic Instruments

<u>Indication Set at % of Full Screen</u>	<u>dB Control Change (a)</u>	<u>Indication Limits % of Full Screen</u>
80	- 6 dB	32 to 48
80	-12 dB	16 to 24
40	+ 6 dB	64 to 96
20	+12 dB	64 to 96

(a) Minus denotes decrease in amplitude; plus denotes increase.

Note: The settings and readings must be estimated to the nearest 1% of full screen.

Calibration Verification

A complete calibration is required at the beginning of each examination; system calibration checks verifying the distance-amplitude correction (DAC) curve and the sweep range calibration must be done at the start and finish of every examination, with any change in personnel, and at least every 4 hours during an examination.

Code Case N-211 waives the personnel change provision in the case of examinations performed using programmed, automated equipment (now in

T-432.1.2). Code cases N-234 and N-235 permit extension of calibration check to 12 hours for automated systems regardless of changes in personnel.

Any change in DAC curve in excess of 20% (2 dB) invalidates all examinations since the last calibration.

The extent of the information that must be recorded can be seen from the list of items required to be included in the piping examination calibration data sheet (III-3500):

- (a) Calibration sheet identification and date;
- (b) Examination personnel;
- (c) Examination procedure number and revision;
- (d) Basic calibration block identity;
- (e) Ultrasonic instrument identity and serial number;
- (f) Beam Angle, couplant, and mode of wave propagation in the material;
- (g) Orientation of search unit with respect to the pipe (longitudinal or circumferential);
- (h) Search unit identity: frequency, size, and Manufacturer's serial number;
- (i) Special search units, wedges, shoe type, or saddle's identity, if used;
- (j) Search unit cable type and length;
- (k) Times of initial calibration and subsequent calibration checks;
- (l) Amplitudes and sweep readings obtained from the calibration reflectors.

If an electronic DAC is being used, a second record shall be made of the resultant amplitudes and sweep readings obtained from the calibration reflectors.

Examination (I-2000, T-441, III-2000)

The basic examination criteria are, for vessels, pulse-echo straight beam and angle beams; and, for piping, pulse-echo angle beams. There are specific

requirements regarding search units (transducers), and on the rate of search unit movement, which cannot exceed 6 in./sec (III-2420). Note that other instrument and search unit characteristic such as transducer size and frequency are not specified.

Surface Preparation (T-441.2, III-4200). Examination surfaces should be free of irregularities, loose/foreign material, or coatings which might interfere with UT.

Examination Coverage (T-425, III-2410). To assure complete coverage of the materials), each pass of the search unit shall overlap a minimum of 10% of the transducer piezoelectric element dimension perpendicular to the direction of scan.

Identification of Examination Areas (T-441, III-4300). Special attention is given to locating all welds and marking them appropriately. Definitive reference systems are required for both pressure vessel and piping.

A general method for pressure vessels is described in I-6231 and Appendix A of Article 4; however, a different method may be utilized so long as it meets the above requirements. Similarly, III-Supplement 2 describes a reference system for piping.

General (I-2000, T-441, III-4400). The volumes of weld and base metal to be examined for both pressure vessels and piping are indicated in the following figures. In the case of pressure vessels, straight beam and 45° and 60° angle beams are used with the option of other pairs of angle beams provided the difference between angles is at least 15°. In piping the preferred angle is 45° with similar options available.

Extent of Scanning. Wherever feasible, the scanning of the examination volume shall be carried out from both sides of the weld on the same surface. Where configuration or adjacent parts of the component are such that scanning from both sides is not practicable, this fact shall be included in the report of the examination. The examination volume shall be scanned by angle beams directed both at right angles to the weld axis and along the weld axis. Wherever feasible, each examination shall be conducted in both directions (approaching the weld from both directions and parallel to the weld in both directions). These examinations shall be conducted using two beam angles from

each direction (nominal angles of 45° and 60°). This results in nine distinct scans of a typical vessel main seam. Examples of scanning volumes are given in Figures 14.2.7-14.2.9 (IWB-3511.1(a), 3511.1(b), and 3514.1(a)).

Wherever possible the entire examination volume shall be scanned. This includes adjacent base material for a distance on each side of the weld equal to one-half the weld thickness. Where component configuration precludes or limits scanning, this fact shall be included in the report of the examination.

Sensitivity level is usually set at twice the reference level for scanning unless an electron DAC is used; however, indications, when found, are recorded at the reference level gain setting:

- reflectors parallel to the weld seam -- the angle beam examination of the weld and HAZ shall be done by a full Vee path from one side or a one-half Vee path from two sides of the weld, where practicable.

FIGURE 14.2.7 (IWB-3511.1(a)). Vessel Shell Circumferential Weld Joints

FIGURE 14.2.8 (IWB-3511.1(b)). Vessel Shell Longitudinal Weld Joints

When examination is limited to one-half Vee path from one or both sides of the weld, the weld and 1/2 in. base material on each side of the weld shall be examined by a surface method.

- reflectors transverse to the weld seam -- The angle beam examination for reflectors transverse to the weld shall be done on the weld crown on a single scan path to examine the weld root by one half Vee path in two directions along the weld.

Scanning for planar and laminar reflectors is conducted with a longitudinal beam in accordance with I-4500 calibration.

Scanning patterns for nozzle welds and adjacent base material will vary according to whether the examination is conducted from inside or outside the nozzle. With an examination from inside the nozzle, a straight beam normal to the plane of the weld may be substituted for the two angle beams normal to the weld seam.

NOM PIPE SIZE LESS THAN 4 IN.

NOM PIPE SIZE 4 IN. AND GREATER

FIGURE 14.2.9 (IWB-3514.1(a)). Similar and Dissimilar Metal Welds in Piping

Sizing of Reflectors. I-6300 (T-441.8) is the part of these requirements that constitutes a really significant departure from other Code UT requirements. We have based our requirements on the "6-dB drop" or "half-amplitude" technique; however, we have modified this several times based on cumulative experience and UT system limitations. A lower threshold for recording data has been stabilized as 50% DAC; however, we have, in Article 4 of Section V, adopted 50% of max. amplitude as the sizing point for reflector signal amplitudes exceeding DAC.

It is appropriate to note here that recording the data is all that we require of the UT operator; unlike the other Code UT requirements, we do not require him to "determine the shape, identity, and location" of all reflectors producing a response greater than 20% of the reference level. The Section XI acceptance standards are based on the "worst case" flaw, a crack, so all reflectors, whether they be from slag, lack-of-fusion, crack, or whatever, are sized as to length and through-thickness dimensions, and judged against the same criteria.

With Appendix III, the volume subject to examination is the weld and 1/4 in. on each side of the weld. In other words, we think that any threat to pressure boundary integrity will be concentrated in that area. However, the causes of confusion due to joint geometry occur in the same area. Code-allowable mismatch, off-set, counterbore, and ID and OD weld convexity are all similar dimensionally to the Section XI weld acceptance standards, and all can produce ultrasonic reflections.

Piping weld examination may be conducted with a single angle-beam transducer. Straight beam and a second beam angle may be used but are not required.

Wherever possible, the entire examination volume is scanned. Where component configuration precludes or limits scanning, this fact shall be included in the report of the examination.

For reflectors parallel to the weld seam, the angle-beam examination of the weld and HAZ is done by a full Vee path from one side or a one-half Vee path from two sides of the weld, where practicable.

For reflectors transverse to the weld seam, the angle-beam examination for reflectors transverse to the weld is done on the weld crown on a single scan path to examine the weld root by one-half Vee path in two directions along the weld.

Data must be recorded for reflectors exceeding 50% of DAC based on the ID and OD notches. We ask that the peak amplitude and the data to determine the location from which you are getting the peak amplitude be recorded, and also the positions where you cross the DAC amplitude.

What Do You Do Where Appendices I and III and Article 4 Do Not Apply?

Article 5 of Section V is a set of general requirements for all ultrasonic examinations called for in the various sections of the Code, and not just Section XI. It includes general procedures for calibration and of flaw detection, but does not include flaw sizing criteria. Typically, determination of flaw shape, identity, and location is the responsibility of the operator. It has recently been rewritten and, as it appears in the Winter 1978 Addenda, is considerably more definitive than before:

Equipment calibration:	T-522 similar to Section XI Appendices.
Personnel training:	Per referencing Code; minimum, SNT-TC-1A 1975.
Written procedure:	Per referencing Code.
Calibration blocks:	For welds (T-543) a similar material is required; 3 side-drilled hole calibration reflectors are required where there was one before; block thickness requirement is the same as Appendix I or Article 4
Calibration:	T-544 provides for a distance-amplitude correction and no longer calls for calibration checks using a "transfer method."
Examination:	One angle beam from 4 directions. Straight beam where angle beam cannot be performed.
Data reporting as modified by IWA-2232(b):	The operator must record all indications producing a response greater than the 50% DAC, size flaws based on 100% DAC.

14.2.3 Records (I-6000, T-470, III-4500)

For each ultrasonic examination the following information shall be identified and recorded:

- (a) procedure
- (b) ultrasonic examination system (equipment)
- (c) examination personnel
- (d) calibration sheet identity
- (e) weld identification and location
- (f) surface from which examination is conducted.

For each inservice inspection, the following information shall be identified and recorded in addition to the information on the individual examinations required above:

- (a) ultrasonic examination procedure (I-1300)
- (b) calibration blocks used (I-3100)
- (c) examination system calibrations (I-4200)
- (d) weld identification plan (I-6200)
- (e) record of reflectors (I-6300, I-6400)
- (f) surface from which examination is conducted.
- (g) record of indications or of volume free of indications. For each indication that equals or exceeds the reference level of 50% DAC, the search unit location and orientation, and the following information, shall be recorded:
 - (1) peak amplitude as either dB from the reference level, or as a percent of DAC; sweep reading in reflector; search unit position; search unit location; sound beam direction per III-4330;
 - (2) minimum sweep reading to reflector and position of search unit at reference level amplitude;
 - (3) maximum sweep reading to reflector and position of search unit at the reference level amplitude;
 - (4) search unit positions or locations parallel to the reflector at the end points where the reflector amplitude equals the reference level (length of reflector).
- (h) date and time period of examination.

The complete report of each examination must be submitted to the local enforcement authority within 90 days of completion, including identification of procedures and equipment sufficient to permit duplication at a later date.

Illustrative Example of Data Recording (I-6330)

When scanning perpendicular to the weld, a reflector is found which exceeds 50% DAC. Position the search unit to give the maximum amplitude from the reflector. Figure 14.2.10 (I-6350) is an illustration of the maximum amplitude scan and tabulation of that data with additional scan data that might be taken on a reflector in a longitudinal weld:

- (a) Read and record the maximum amplitude in percent of DAC.
- (b) Read and record the distance from surface to reflector (at the left side of the indication on the sweep).
- (c) Read and record the position of the search unit on the indexing strip at the reference line.
- (d) Read and record the location of the indication at the beam centerline intersection with reference line from the weld layout reference points in inches (for longitudinal or circumferential welds) or in azimuth degrees (for nozzle welds).

Move the search unit toward the reflector until the amplitude falls to half the maximum. Read and record the following:

- (e) minimum distance from surface and
- (f) minimum position.

Move the search unit away from the reflector past the maximum amplitude position until the amplitude falls to half of the maximum amplitude. Read and record the following:

- (g) maximum distance from surface and
- (h) maximum position.

Subtract the minimum distance from surface from the maximum and divide by the through thickness reading on the sweep. Record as

- (i) depth in percent of T.

Subtract the maximum distance from surface from the through thickness reading (or use the minimum depth, whichever gives the smaller number), and divide the number by the through thickness reading on the sweep. Record as

- (j) distance from surface in percent of T.

FIGURE 14.2.10 (I-6350). Reflector Reading

Data Tabulation for Figure 14.2.10 (I-6350)

(a)	(b)	(c)	(d)	(e)	(f)	(g)	(h)	(i)		(j)	COMPUTATION OR REMARKS
				50% DAC				% OF t			
				MINIMUM		MAXIMUM		DEPTH	DISTANCE FROM SURFACE		
MAXIMUM % DAC	SWEEP READING	POSITION, in.	LOCATION, in.	SWEEP READING	POSITION	SWEEP READING	POSITION				
120	4.0	4.6	23	3.6	4.2	4.4	5.2	10	45	4.4 - 3.6 8.0	
80	4.0	4.6	22.1	3.8	4.3	4.3	4.8	5	47.5	4.2 - 3.8 8.0	
50	4.0	4.6	21.7	---	---	---	---	0	50	---	
90	4.1	4.7	23.9	3.7	4.3	4.3	5.2	7.5	46	4.3 - 3.7 8.0	
70	4.1	4.7	24.8	3.9	4.4	4.3	5.2	5	46	4.3 - 3.9 8.0	
50	4.2	4.6	25	---	---	---	---	0	50	---	
										DEPTH: 10% OF t	
										LENGTH: 3.3 in.	

An examination of the PVRC and PISC results in Chapter 3 highlights the importance of calibration and the operator. The poorly defined calibration and examination procedures of the early PVRC tests were mirrored in the poor reliability of detection of flaws. Plate No. 201 is the best example of these limitations. The results with the unclad block were fair to poor. With the clad block they were poor to nonexistent.

A tightening up of the calibration and examination requirements highlighted the operator variable. The reliabilities of flaw detection ranged from excellent to poor with the operator being the major variable. This was seen for the PVRC test block data and is buried in the PISC data. Since the PISC data on detection reliability are averaged, it is not apparent that a few teams did very well while most did relatively poorly. A definite weakness in the ASME V, XI, UT techniques is the degree of subjectivity introduced by the operator. If it were possible to achieve results consistent with top operators performance, the existing UT procedures would be quite adequate. Unfortunately, this has not been achieved. The Air Force study cited in Chapter 13^(13.6.17) clearly underlines the operator variability. It also confirms that the difference between the very good and the average operator consists of a series of imponderables not lending themselves to statistical analysis. This difference stresses the importance of reducing subjective factors through modification of the current ASME UT procedures even if it means sacrificing to a limited degree the results obtained by the outstanding operator.

Calibration blocks represent a significant variable with approaches based on side-drilled holes, flat-bottomed holes, notches or even real cracks. Acceptable results have been obtained with all calibration approaches, providing a rigorous procedure has been developed and is followed by the operator. Some studies^(2.2.3) indicate that notches more closely resemble cracks and are preferred in conjunction with holes. Attention to detail appears to be the significant factor.

ASME XI UT is based on a comparison to the distance-amplitude curve developed during calibration. Originally defects were compared to a 20% DAC curve. Later this was changed to 50% recording and 100% DAC reporting on the basis that 20% DAC resulted in an excessive time for evaluation of geometric indications. While true, work of Taylor and Selby^(2.2.3) reveal that detection reliability also is reduced markedly. While an increase in DAC level reduced time in analyzing geometric effects, it also permitted significant flaws to be unreported. In fact, in at least one instance cracks exceeding 50% through-wall completely around the circumference of piping generated UT signals well below 50% DAC.

Another problem not limited to ASME XI UT procedures is detection of near-surface, near-field flaws. Conventional shear wave is generally unsuccessful in detecting flaws immediately under cladding when examined through cladding. It may be necessary to add requirements, particularly longitudinal wave examination, or utilize some other techniques.

Finally, most conventional UT tends to overestimate small flaw and underestimate large ones. If sizing of flaws was uncoupled from detection, the detection mode could be optimized. Once it has been determined that a real flaw exists near or above the size permitted by the standards, techniques such as holography, SAFT-UT, etc., can be used to establish flaw location, orientation and dimensions.

Regulatory Guide 1.150 was developed in an attempt to improve the reliability of UT. I would classify this and other documents as stop gap approaches aimed at improvement of perceived limitations. In the case of Regulatory Guide 1.150, emphasis is given to instrument drift, calibration, and control of sizing errors. The level of improvement does not appear to be commensurate with the added time required.

Some members of the USNRC have expressed reservations as to the adequacy of later versions of the UT requirements in ASME XI. These reservations culminated in a Regulatory Guide 1.150, "Ultrasonic Testing of Reactor Vessel Welds During Preservice and Inservice Examinations." This document had a major impact on the UT sector of the nuclear industry. Great quantities of comments were generated, usually citing why the requirements could not be complied with. Finally, a more directed effort was conducted under an ad hoc committee of the electric utility industry chaired by Jack Lance of Yankee Atomic Electric Company. This culminated in a report, "Development of Optimized Reactor Pressure Vessel Inspection Guides," June 1982.

The report contains a point-by-point comparison of the verbatim positions in the Regulatory Guide with the alternates suggested by the ad hoc committee. This comparison is shown in Table 14.4.1 to permit an understanding of both the Regulatory Guide and the industry alternatives. Presumably, there will be a series of meetings with the USNRC to attempt to modify the Regulatory Guide. How successful they will be in modifying it only time will tell.

TABLE 14.4.1. Recommended Changes to Regulatory Guide 1.150

Current Regulatory Position

Ultrasonic examination of reactor vessel welds should be performed according to the requirements of Section XI of the ASME B PV Code, as referenced in the Safety Analysis Report (SAR) and its amendments, supplemented by the following:

1. INSTRUMENT PERFORMANCE CHECKS

The checks described in paragraphs 1.2 through 1.5 should be made for any UT system used for the recording and sizing of reflectors in accordance with regulatory position 6 and for reflectors that exceed the Code-allowable criteria.

1.1 Frequency of Checks

As a minimum, these checks should be verified within 1 day before and within 1 day after examining all the welds that need to be examined in a reactor pressure vessel during one outage. Pulse shape and noise suppression controls should remain at the same setting during examination and calibration.

1.2 Screen Height Linearity

Screen height linearity of the ultrasonic instrument should be determined according to the mandatory Appendix I to Article 4, of Section V the ASME Code, within the time limits specified in regulatory position 1.1.

1.3 Amplitude Control Linearity

Amplitude control linearity should be determined according to the mandatory Appendix 4, Section V of the

Recommended Changes

Ultrasonic examination of reactor vessel welds should be performed according to the requirements of Section XI of the ASME B PV Code, as referenced in the Safety Analysis Report (SAR) and its amendments, supplemented by the following:

1. INSPECTION SYSTEM PERFORMANCE CHECKS

The conduct of a quality examination requires that the performance characteristics of inspection system (instruments, cable, transducer, etc.) used be well defined and documented. This is particularly true for situations which require comparisons of examination results generated during successive examinations on the same components.

The checks described in paragraphs 1.1 and 1.2 should be made for any UT system used for inspection of reactor pressure vessel welds.

The field performance checks described in 1.2 (with the possible exception of 1.2.c.) will be conducted on the basic calibration block.

1.1 Pre-exam Performance Checks

a. Frequency of checks

These checks should be verified within 6 months before reactor pressure vessel examinations performed during one outage. Pulse shape and noise suppression controls should remain at the same settings during calibration and examination.

ASME Code, 1977 edition, within the time limits specified in regulatory position 1.1

1.4 Frequency-Amplitude Curve

A photographic record of the frequency-amplitude curve should be obtained. This record should be available for comparison at the inspection site for the next two successive inspections of the same volume. The reflector used in generating the frequency-amplitude curves as well as the electronic system (i.e., the basic ultrasonic instrument, gating, form of gated signal, and spectrum analysis equipment) and how it is used to capture the frequency-amplitude information should be documented.

1.5 Pulse Shape

A photographic record of the unloaded initial pulse against a calibrated time base should be obtained. The time base and voltage values should be identified and recorded on the horizontal and vertical axis of the above photographic record of the initial pulse. The method used in obtaining the pulse shape photograph, including the test point at which it is obtained, should be documented.

b. RF Waveform

A record of the RF (radiofrequency) pulse waveform from a reference reflector should be obtained for each search unit used in the examination in a manner which will provide frequency amplitude information. At the highest amplitude portion of the beam, the RF return signal should be recorded before it has been rectified or conditioned for display. The reflector used in generating the RF return signal as well as the electronic system (i.e., the basic ultrasonic instrument, gating, and form of gated signal) should be documented. These records should be used for comparison with previous records.

1.2 Field Performance Checks

a. Frequency of Checks

As a minimum, these checks should be verified within 1 day before and within 1 day after examining all the welds that need to be examined in a reactor pressure vessel during one outage. Pulse shape and noise suppression controls should remain at the same setting during examination and calibration.

b. Instrument Sensitivity During Linearity Checks

The initial instrument sensitivity during the performance of 1.2.d and 1.2.e should be such that it falls at the calibration sensitivity or at some point between the calibration sensitivity and the scanning sensitivity.

c. RF Waveform

A record of the RF (radiofrequency) pulse waveform from a reference reflector should be obtained

and recorded in a manner that will permit extraction of frequency amplitude information. At the highest amplitude portion of the beam, the RF return signal should be recorded before it has been rectified or conditioned for display. This should be determined on the same reflector as that used in 1.1.b above. This record should be retained for future reference.

d. Screen Height Linearity

Screen height linearity of the ultrasonic instrument should be determined according to the mandatory Appendix I to Article 4, Section V of the ASME Code or Appendix I to Section XI of the ASME Code.

e. Amplitude Control Linearity

Amplitude control should be determined according to the mandatory Appendix II of Article 4, Section V of the ASME Code or Appendix I to Section XI of the ASME Code.

f. Angle Beam Profile
Characterization

The vertical beam profile should be determined for each search unit used during the examination by a procedure similar to that outlined in nonmandatory Appendix 8-60, Article 4, Section V of the ASME Code or Appendix I to Section XI of the ASME Code. Beam profile curves should be determined at different depths to cover the thicknesses of materials to be examined. Interpolation may be used to obtain beam profile correction for assessing flaws at intermediate depths for which beam profile has not been determined.

Beam profile measurements should be made at the sensitivity required

for sizing. For example, sizing to a 20% DAC criteria requires that the beam profile be determined at 20% DAC.

2. CALIBRATION

System calibration should be checked to verify the DAC curve and the sweep range calibration per non-mandatory Appendix B, Article 4, Section V of the ASME Code, as a minimum, before and after each RPV examination (or each week in which it is in use, whichever is less) or each time any component (e.g., transducer, cable, connector, pulser, or receiver) in the examination system is changed. Where possible, the same calibration block should be used for successive inservice examinations of the same RPV. The calibration side holes in the basic calibration block and the block surface should be protected so that their characteristics do not change during storage. These side holes or the block surface should not be modified in any way (e.g., by polishing) between successive examinations. If the block surface or the calibration reflector holes have been polished by any chemical or mechanical means, this fact should be recorded.

2.1 Calibration for Manual Scanning

For manual scanning for the sizing of flaws, static calibration may be used if sizing is performed using a static transducer. When signals are maximized during calibration, they should also be maximized during sizing. For manual scanning for the detection of flaws, reference hole detection should be shown at scanning speed and detection level set accordingly (from the dynamic DAC).

2. CALIBRATION

System calibration should be checked to verify the DAC curve and the sweep range calibration per non-mandatory Appendix B, Article 4, Section V of the ASME Code; Appendix I of Section XI; or the plant's Technical Specifications before and after each RPV examination (or each week in which it is in use, whichever is less) or each time any component (e.g., transducer, cable, connector, pulser, or receiver) in the examination system is changed. Where possible, the same calibration block should be used for successive inservice examinations of the same RPV.

2.1 Calibration for Manual Scanning

For manual sizing of flaws, static calibration may be used if sizing is performed using a static transducer. When signals are maximized during calibration, they should also be maximized during sizing. For manual scanning for the detection of flaws, reference hole detection should be shown at scanning speed and detection level set accordingly.

2.2 Calibration for Mechanized Scanning

When flaw detection and sizing are to be done by mechanized equipment, the calibration should be performed using the following guidelines:

a. Calibration speed should be at or higher than the scanning speed.

b. The direction of transducer movement during calibration should be the same as the direction during scanning unless 1) it can be shown that the change in scanning direction does not make a difference in the sensitivity and vibration background noise received from the search unit or 2) these differences are taken into account by a correction factor.

c. For mechanized scanning, signals should not be maximized during the establishment of the DAC curve.

d. One of the following alternative guidelines should be followed for establishing the DAC curve:

1) The DAC curve should be established using a moving transducer mounted on the mechanism that will be used for examination of the component.

2) Correction factors between dynamic and static response should be established using full-scale mockups.

3) Correction factors should be established using models and taking scaling factors into consideration (assumed scaling relationship should be verified).

2.2 Calibration for Mechanized Scanning

When flaw detection is to be done by mechanized equipment, the calibration should be performed using the following guidelines.

a. Calibration speed should be at or higher than the scanning speed.

b. The direction of transducer movement (forward and backward) during calibration to establish the DAC curve should be the same direction during scanning unless it can be shown that a change in scanning direction does not reduce flaw detection capability.

c. One of the following alternative guidelines should be followed for establishing the DAC curve:

1) The DAC curve should be established using either a moving transducer mounted on the mechanism that will be used for examination of the component, or a mechanism that duplicates the critical factors (e.g., transducer mounting, weight, pivot points, couplant) present in the scanning mechanism.

2) Correction factors between dynamic and static response should be established using the basic calibration block or full-scale mockups.

3) Correction factors should be established using models and taking scale factors into consideration (assumed scaling relationship should be verified).

4) Correction factors between dynamic and static response should be established from the indications that are found during examination for sizing. For detection of flaws during the initial scan, correction factors may be assumed based on engineering judgment. If assumed correction factors are used for detection, these factors should later be confirmed on indications from flaws in the vessel during the examination. Deviation from the assumed value may suggest reexamining the data.

2.3 Calibration Checks

If an EBS is used for calibration check, the following should apply:

a. The significant DAC percentage level used for the detection and sizing of indications should be reduced to take into account the maximum error that could be introduced in the system by the variation of resistance or leakage in the connectors or other causes.

b. Calibration checks should be performed on the complete connected system (e.g., transducer and cables should not be checked separately).

c. Measures should be taken to ensure that the different variables such as temperature, vibration, and shock limits for which the EBS error band is determined are not exceeded during transport, use, storage, etc.

d. When a universal calibration block is used and some or all of the reference holes are larger than the reflector holes at comparable depths recommended by Article 4, Section V, of the ASME Code, 1980 edition, a

2.3 Calibration Confirmation

Calibration confirmation performed as mid-shift or interim confirmation between on-site calibrations should comply with stability requirements in T-433, Article 4, Section V of the ASME Code.

When an electronic simulator is used for on-site calibration confirmation after a Code-required block calibration performed off-site, the following should also apply:

a. Complete system performance should be maintained stable prior to off-site calibrations and on-site calibration confirmation by use of target reflectors. The target reflectors should be mounted with identical physical displacement in both the off-site calibration facilities and the on-site mechanized equipment. Each on-site periodic calibration should be preceded by complete system performance verification using a minimum of 2 target reflectors separated by a distance representing 75% of maximum thickness to be examined.

b. Written records of calibrations should be established for both target reflector responses and Code calibration block DAC curves for each transducer. These written records may be used to monitor drift since the original recorded calibration.

c. Measures should be taken to ensure that the different variables such as temperature, vibration, and shock limits are minimized by controlling packaging, handling, and storage.

correction factor should be used to adjust the DAC level to compensate for the larger reflector holes. Also, if the reactor pressure vessel has been previously examined by using a conventional block, a ratio between the DAC curves obtained from the two blocks should be noted (for reference) with the significant indications data.

3. NEAR-SURFACE EXAMINATION AND SURFACE RESOLUTION

The capability to effectively detect defects near the front and back surfaces of the actual component should be estimated. The results should be reported with the report of abnormal degradation of reactor pressure boundary in accordance with the recommendation of regulatory position 2.a(3) of Regulatory Guide 1.16. In determining this capability, the effect of the following factors should also be considered:

a. If an electronic gate is used, the time of start and stop of the control points of the electronic gate should be related to the volume of material near each surface that is not being examined.

2.4 Calibration Blocks

Calibration blocks should comply with IWA-2200 of Section XI of the ASME Code. When a universal calibration block or a new conventional block is used, a ratio between the DAC curves obtained from the original block and from the new block should be noted (for reference) to provide for a meaningful comparison of previous and current data.

The calibration side-drilled holes in the basic calibration block and the block surface should be protected so that their characteristics do not change during storage. These side-drilled holes or the block surface should not be modified in any way (e.g., by polishing) between successive examinations. If the block surface or the calibration reflector holes have been polished by any chemical or mechanical means, this fact should be recorded.

3. EXAMINATION

The scope and extent of the ultrasonic examinations should comply with IWA-2000, Section XI of the ASME Code.

If an electronic gate is used, the start and stop control points should include the entire required thickness including the material near each surface.

3.1 Near Surface

The capability to effectively detect defects at the internal clad/base metal interface shall be considered acceptable if the examination procedure(s) or technique(s) meet the requirements of Section 6.0 of this document and demonstrate the following:

b. The decay time, in terms of metal path distance, of the initial pulse and of the pulse reflections at the front and back surface should be considered.

c. The disturbance created by the clad-weld-metal interface with the parent metal at the front or the back surface should be related to the volume of material near the interface that is not being examined.

d. The disturbance created by front and back metal surface roughness should be related to the volume of material near each surface that is not being examined.

a. All examination procedures should include the use of the 2% notch which penetrates the internal (clad) surface of the calibration blocks defined by Section XI, Appendix I Figure 1-3131 or Section V, Article 4, Figure T-434.1.

b. An alternate reflector, other than the 2% notch described above, may be used provided: 1) that it is located at the clad/base metal interface or at an equivalent distance from the surface, 2) that it does not exceed the maximum allowable defect size, and 3) that equivalent or superior results can be demonstrated.

c. The examination procedure(s) should provide for volumetric examination of at least 1 inch of metal as measured perpendicular to the nominal location of the base metal-cladding interface.

3.2 Scanning Weld-Metal Interface

The beam angles used to scan welds should be based on the geometry of the weld/parent metal interface. Where feasible for welds such as those identified in Section T-441.4.2 of Article 4, Section V of the ASME Code, at least one angle should be such that the beam is perpendicular ($\pm 15^\circ$ to the perpendicular) to the weld/parent metal interface, or it should be demonstrated that unfavorably oriented planar flaws can be detected by the UT technique being used. If this is not feasible, use of alternative volumetric NDE techniques, as permitted by the ASME Code, should be considered.

4. BEAM PROFILE

The beam profile should be determined if any recordable flaws are detected. This should be done for each search unit used during the examination by a procedure similar to that outlined in the nonmandatory Appendix B(B-60), Article 4, Section V of the ASME Code, 1980 edition, for determining beam spread. Beam profile curves should be determined for each of the holes in the basic calibration block. Interpolation may be used to obtain beam profile correction for assessing flaws at intermediate depths for which the beam profile has not been determined.

5. SCANNING WELD-METAL INTERFACE

The beam angles used to scan welds should be based on the geometry of the weld/parent-metal interface. At least one of these angles should be such that the beam is almost perpendicular ($\pm 15^\circ$ to the perpendicular) to the weld/parent-metal interface unless it can be demonstrated that unfavorably oriented planar flaws can be detected by the UT technique being used. Otherwise, use of alternative volumetric NDE techniques, as permitted by the ASME Code, should be considered. Alternative NDE techniques may be considered to include high-intensity radiography or tandem-probe ultrasonic examination of the weld-metal interface.

6. SIZING

Indications from geometric sources need not be recorded.

4. BEAM PROFILE

(Delete entire paragraph. This section included in Recommended Change 1.2.f., Angle Beam Profile Characterization.)

5. SCANNING WELD-METAL INTERFACE

(Delete entire paragraph. This section included in Recommended Change 3.2, Scanning Weld-Metal Interface.)

6. RECORDING AND SIZING

The capability to detect, record and size the flaws delineated by Section XI, IWB-3500 should be demonstrated. The measurement tolerance established should be applied when sizing flaws detected and

6.1 Traveling Indications

Indications that travel on the horizontal baseline of the scope for a distance greater than indications from the calibration holes (at 20% DAC amplitude) should be recorded. Indications that travel should be recorded and sized at 20% DAC. Where the indication is sized at 20% DAC, this size may be corrected by subtracting for the beam width in the through-thickness direction obtained from the calibration hole (between 20% DAC points) that is at a depth similar to the flaw depth. If the indication exceeds 50% DAC, the size should be recorded by measuring the distance between 50% DAC levels without using the beam-width correction. The determined size should be the larger of the two.

6.2 Nontraveling Indications

Nontraveling indications above 20% DAC level that persist for a scanning distance of more than 1 inch plus the beam spread between 20% DAC points (as defined by non-mandatory Appendix B, Article 4, Section V of the ASME Code, 1977 edition) should be considered significant. The size of these flaws should be determined by measuring the distance between points at 50% DAC and between points at 20% DAC where the beam-width correction is made only for the 20% DAC size. The recorded size of the flaw would be the larger of the two determinations. If it can be adequately demonstrated that a nontraveling indication is from a geometric source (and not a flaw), there is no need to record that indication.

The following information should also be recorded for indications

recorded during scanning (see paragraph 7.a).

6.1 Geometric Sources

Indications from geometric sources need not be sized. Recording of these indications should be at 50% DAC. Paragraphs 6.2(a) and (b) do not apply.

6.2 Indications with Changing Metal Path

a. Indications that change metal path distances (indicating through-wall dimension) when scanned in accordance with the requirements of ASME Section XI, for a distance greater than that recorded from the calibration reflector should be recorded.

b. Reflectors which are at metal paths representing 25% and greater of the through-wall thickness of the vessel wall measured from the inner surface should be recorded in accordance with the requirements of ASME Section XI and characterized at 50% DAC.

c. Reflectors which are within the inner 25% of the through-wall thickness should be recorded at 20% DAC. Characterization should be per the demonstrated methods under paragraph 6.0. Where the indication is sized at 20% DAC, this size may be corrected by subtracting the beam width in the through-thickness direction obtained from the calibration hole (between 20% DAC points) which is at a depth similar to the flaw depth. If the indication exceeds 50% DAC, the size should be recorded by measuring the distance between 50% DAC levels without using the beam-width correction. The determined size should be the larger of the two.

that are reportable according to this regulatory position:

a. Indications should be recorded at scan intervals no greater than one-fourth inch.

b. The recorded information should include the indication travel (metal path length) and the transducer position for the 10%, 20%, 50%, and 100% DAC and the maximum amplitude of the signal.

6.3 Indications without Changing Metal Path

Precautionary Note.

Indications lying parallel to welds may appear as nontraveling (without changing metal path) when scanned by parallel moving transducers whose beams are aimed normal to the weld, i.e. at 90°. Multiple scans, however, may reveal that these indications are traveling indications. If so, recording and sizing are to be done in accordance with paragraph 6.2.

a. Indications which do not change metal path distance when scanned in accordance with the requirements of ASME Section XI and are between the inner 25% of the through-wall dimension and the outside surface should be recorded when any continuous dimension exceeds 1 inch in the scanning direction.

b. If the indication falls within the inner 25% of the through-wall dimensions, it should be recorded at 20% DAC and evaluated at 50% DAC.

6.4 Additional Recording Criteria

The following information should also be recorded for indications that are reportable according to this regulatory position:

a. Indications should be recorded at scan intervals no greater than one-fourth inch.

b. The recorded information should include the indication travel (metal path distance) and the transducer position for 20% (where applicable), 50%, and 100% DAC, and the maximum amplitude of the signal.

c. When multi-channel equipment is used in the examination system such that all examination displays are not available for simultaneous viewing, an electronic gating system should be used which will provide on-line reproducible, recording information, regarding metal path, amplitude and position of all indications exceeding a preset level. The preset level should be the minimum recording level required.

7. REPORTING OF RESULTS

Records obtained while following the recommendations of regulatory positions 1.2, 3, 5, and 6, along with discussions and explanations, if any, should be kept available at the site for examination by the NRC staff. If the size of an indication, as determined in regulatory positions 6.1 or 6.2, equals or exceeds the allowable limits of Section XI of the ASME Code, the indications should be reported as abnormal degradation of reactor pressure boundary in accordance with the recommendation of regulatory position 2.a(3) of Regulatory Guide 1.16.

Along with the report of ultrasonic examination test results, the following information should also be included:

a. The best estimate of the error band in sizing the flaws and the basis for this estimate should be given.

b. The best estimate of the portion of the volume required to be examined by the ASME Code that has not been effectively examined such as volumes of material near each surface because of near-field or other effects, volumes near interfaces between cladding and parent

7. REPORTING OF RESULTS

Records obtained while following the recommendations of regulatory positions 1.2, 3, and 6, along with discussions and explanations, if any, should be kept available at the site. If the size of an indication, as determined in regulatory positions 6.2 or 6.3, exceeds the allowable limits of Section XI of the ASME Code, the indications should be reported as abnormal degradation of reactor pressure boundary in accordance with the recommendation of regulatory position 2.a(3) of Regulatory Guide 1.16.

Along with the report of ultrasonic examination test results, the following information should also be included:

a. The best estimate of the tolerances in sizing the flaws at the sensitivity required in Section 6 and the basis for this estimate.

This estimate may be determined in part by the use of additional reflectors in the standard calibration block.

b. A description of the technique used to qualify the effectiveness of the examination procedure,

metal, volumes shadowed by laminar material defects, volumes shadowed by part geometry, volumes inaccessible to the transducer, volumes affected by electronic gating, and volumes near the surface opposite the transducer.^(a)

c. The material volume that has not been effectively examined by the use of the above procedures may be examined by alternative effective volumetric NDE techniques. If one of these alternative NDE techniques is a variation of UT, recommendations of regulatory positions 1 and 3 should apply. A description of the techniques used should be included in the report. If other volumetric techniques or variations of UT are used as indicated in regulatory position 5, the effectiveness of these techniques should be demonstrated and the procedures reported for review by the NRC staff.

(a) It should be noted that the licensee is required to apply for relief from impractical ASME Code requirements according to §50.55a of 10 CFR. If the licensee is committed to examine a weld as per the inspection plan in the plant SAR, the licensee is required to file an amendment when the commitments made in the SAR cannot be met.

including, as a minimum, material, section thickness and reflectors.

c. The best estimate of the portion of the volume required to be examined by the ASME Code that has not been effectively examined such as volumes of material near each surface because of near-field or other effects, volumes near interfaces between cladding and parent metal, volumes shadowed by laminar material defects, volumes shadowed by part geometry, volumes inaccessible to the transducer, volumes affected by electronic gating, and volumes near the surface opposite the transducer.^(a)

Sketches and/or descriptions of the tools, fixtures and component geometry which contribute to incomplete coverage should be included.

d. When other volumetric techniques are used, a description of the techniques used should be included in the report.

(a) It should be noted that the licensee is required to apply for relief from impractical ASME Code requirements according to §50.55a of 10 CFR. If the licensee is committed to examine a weld as per the inspection plan in the plant SAR, the licensee is required to file an amendment when the commitments made in the SAR cannot be met.

The following are some suggested changes for improving NDE:

- Achieve a better understanding of factors responsible for differentiating the average from the excellent operator. Establish whether these factors can be quantified and used to improve operators.
- As an alternate to improving the operator, consider automatic or semi-automatic approaches that will remove the operator from the decision process. This appears to be more applicable to the reactor pressure vessel than to piping.
- Consider incorporation into the Code, or making available as a supplement to the Code, techniques such as longitudinal wave that are capable of detecting near-surface flaws.
- Decouple sizing from detection; once it has been established that the signal results from a flaw, a technique best capable of sizing and locating it in a given geometry can be utilized.
- Consider modifying DAC reporting levels on the basis of potential significance of a flaw. The mid-thickness flaw has little or no safety significance and could be checked at 100% or 200% DAC. On the other hand, near-surface flaws have definite fracture mechanics significance and could use 20% DAC.
- Continue to emphasize the need for well-defined calibration procedures. This might entail further optimization of calibration blocks.
- Recognize that pulse-amplitude techniques have inherent limitations and consider use of more of the information in the signal spectrum. The outstanding operator probably is doing that to a degree now.

CHAPTER 15

CONCLUSIONS AND RECOMMENDATIONS

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CHAPTER 15

CONCLUSIONS AND RECOMMENDATIONS

15.1

INTRODUCTION

This chapter is a distillation of the preceding 17. Significant conclusions and recommendations are repeated here, including a locator to permit referral to the original source. It is hoped that the repetitive nature of the items which are grouped to accentuate this repetitiveness will underscore the significant items. Basically, this chapter is a built-in index pointing toward these items. Nothing new has been included.

The following three somewhat arbitrary categories have been selected to permit discrimination and categorization of the various items:

- Systems Controls—NDE (UT) system parameters relating either to equipment or transducers that control. Obviously, the decision may be somewhat subjective as to whether system parameters control.
- Technique Controls—techniques impressed on the system that influence results. Typically operator influence, calibration, and code or standard defined procedures will control.
- External Factors Controls—variables inherent in the object being examined that control. Obvious examples include flaw orientation and flaw surface characteristics, and material grain size and orientation effects as they influence beam attenuation, skewing, etc.

Obviously, there will be a substantial degree of subjectivity between and within these three categories. I hope readers recognize this subjectivity when they disagree with the location of specific items. All original references occur in the chapters cited. There is a series of bullets grouped on the basis of a common aspect and incorporated under the three major headings.

15.2.1 Beam Angle

- Reliability of detection and sizing of defects using a single combination of transducer beam angle and frequency was substantially lower than had been anticipated (3.6).
- A 45° refracted shear wave yielded better results than 60° shear wave with respect to S/N ratio in austenitic weldments (6.5).
- Selection of incident angles should be optimized for each examination zone in a structure; e.g., near the surface 78° (4.5.2.3.4).
- It is necessary to use different probe angles (70°, 45°, etc.) and to scan from different directions to ensure flaw detection (2.3.1).
- As many beam angles and traverses as economically feasible should be used to minimize inherent limitations in detection (4.7).
- Combinations of angles and frequencies enhance the reliability of detecting and sizing a given defect (3.6).

15.2.2 Frequency

- Frequencies less than 1.8 MHz are relatively unaffected by material parameters. Above 1.8 MHz with the metallurgical structures typical of austenitic stainless steel weldments filtering, distortion and attenuation occurs (6.5).
- A decrease in frequency broadens the beam and increases the radiation pattern response (7.3.1).
- Frequency should be optimized to yield acceptable S/N ratios together with good flaw resolution. Higher frequencies increase noise and decrease S/N ratios. Lower frequencies (<1 MHz) decrease resolution. The optimum range is 1 to 2.25 MHz (13.4.3).
- Two significant parameters accounting for UT equipment variability are the system operating frequency spectrum and the beam profile or radiation pattern of the search unit (4.5.1).

- It is vital to match test equipment and transducers in a frequency range optimum for both. Often this is not done leading to no results or incorrect results (7.3.2).
- UT equipment can meet ASME XI requirements yet have characteristics unknown to the operator which will influence test performance. Examples are frequency response of probes (7.3).
- The two most important parameters are operating frequency spectrum and beam profile for a given material (7.3).
- Increases in frequency are accompanied by decreases in beam spread (8.4).

15.2.3 Beam Spread

- For a high detection probability with adequate S/N ratio, medium divergence probes are preferred. This does not apply to sizing (4.6.4).
- Reduction of beam spread reduces noise level with coarse-grained materials because scatter is a volume effect. Focused probes accomplish this; however, they may introduce other problems (4.5.2.3.4).
- For sizing, a wide sound beam yields better results than does a narrow (focused) sound beam and a wave-front with phase variations (near field/transition field) gives better results than a flat wave front (far-field focus) (6.2.3).

15.2.4 Beam Size

- Conventional UT consistently overestimates flaws whose sizes are less than the focal spot. Larger flaws may be underestimated depending on the sizing technique used (4.6.4).
- Although B and C scans improve estimations of flaw location and orientation, they do not assist in sizing if defects are small relative to beam diameter (7.2.1).

- With flaws smaller than the limit of resolution probe displacement techniques are not accurate because the flaw ends cannot be distinguished (5.3.9).

15.2.5 Pulse

- Short broad pulses are preferred for coarse-grained materials because they suppress reflections from the grains as well as yielding higher resolution (4.5.2.3.4).
- Short pulse probes are an advantage with anisotropic coarse-grained materials because there is less energy scattered along the beam; therefore, less noise (4.5.2.3.4).
- Short broadband pulses will reduce scattering, improve S/N ratio and reduce false signals. Short pulse lengths reduce scattering thus improving S/N ratio. Increased bandwidth reduces false signals because of shorter signal times. This approach also reduces signal amplitude (13.4.3, 7.5.3).
- A pulse behaves as if it consists of a continuous spectrum of sinusoidal waves; as the pulse is reduced in duration the bandwidth increases (8.4).
- Short pulses are better suited to test coarse-grained materials than long pulses (6.5.1).
- Pulse shape may be a factor depending on whether the harmonic signal does or does not occur; a nonharmonic signal is more detectable (4.6.4).

15.2.6 Bandwidth

- Optimum systems for austenitic weldments include a medium bandwidth unit utilizing a tuned pulse capable of multicycle waveform excitation, preferably pitch-catch if applicable (6.5).
- An ideal transducer would be a broadband device having a uniform response in the frequency domain and a delta transfer function in the time domain. Such a unit would have high resolution and faithfully

transduct signals. Unfortunately, real transducers do not behave thus, having low sensitivity so the alternative is a narrow-band unit (7.3.1).

- Narrow-band equipment performs better than wide band for coarse-grained materials (6.5).
- While narrow-band pulses and short pulses have specific advantages, they cannot be combined to yield a better system (6.5).
- Optimum results can be obtained by minimizing equipment variability such as selecting either a tuned or a broadband system but not both (4.7).
- Broadband is less sensitive than narrow band with effects more pronounced in the far field (4.2.4.1).

15.2.7 Signal-to-Noise (S/N) Ratio

- The S/N ratio is proportional to the square root of the bandwidth; therefore, a large bandwidth (short pulse) gives better results than a narrow bandwidth (long pulse) (8.4).
- In defects yielding small amplitudes, spurious indications are the major problem rather than attenuation or sensitivity (6.5).

15.2.8 Transducers/Probes

- Transducer variability is a major problem. Until such variability is resolved the option is calibration and certification (7.3.1.1).
- While very small transducers tend to yield more accurate results, conventional industrial practice is to compromise by using an intermediate size (4.6.4).
- A larger probe is better for a general scan provided weld crown does not force the use of a smaller one (7.5.3.2).
- With the ideal combination of transducer size to defect size it should be possible to accurately estimate flaw size, given that the flaw orientation is accurately known (unlikely). The ideal condition

would be an infinitely small transducer to obtain true orientation assuming knowledge of location (4.6.4).

- A recommendation concerns evaluation of transducers to determine what parameters enhance detection and analyses of IGSCC (4.5.2.3.4).
- Transducers represent a major source of variation. The radiation patterns, intensity and frequency response generally cannot be reproduced within 20% even when the transducers are from the same manufacturers. Also, transducer characteristics decay with usage; the mean useful life is about six months (4.5.1). The variability between classes of transducers is much worse than within classes (4.5.1.1).

15.2.9 Wave Mode

- Shear-wave probes performed significantly better than longitudinal-wave probes for weak signal defects in austenitic steels (6.5).
- Longitudinal waves are generally preferred when material anisotropy is a factor because they are less sensitive to such structures. For example, the shear mode accounts for most of the energy scattered by the grains and enhancing noise level, and there is less skewing with L-waves (4.5.2.3.4).

15.2.10 Miscellany

- Extensive testing of UT equipment and probes indicate a spread in values of 12 dB at two sigma for calibrations against known reflectors (7.3).
- Specialized equipment is needed to improve detection of IGSCC, particularly with regard to enhancing S/N ratios. Use of flawed specimens should help (4.5.2.3.4.).
- A limitation on UT is the zone of low sensitivity near the incident beam surface. This zone is smaller with direct coupling than for immersion. Complementary UT techniques may be needed to examine this region (5.3.9).

- Mode conversion will adversely affect the signals from flaws because of the angular relationships (4.5.2.4).
- Beam misalignment represents a potentially severe fault with frequencies below 6 MHz more susceptible than above. Contact probes are less susceptible than immersion probes. Typically, misalignments may exceed 2.5° (7.3.1).

15.2.11 Focused Probes

- Focused transducers appear to have considerable promise in the sizing of embedded flaws; however, their accuracy in bimetallic and trimetallic welds or in coarse-grained austenitic structures has not been established (6.17).
- Immersion focused probes are less sensitive to surface variability than contact probes so they may be preferred for PWR NDE, particularly for underclad cracks (7.6.2).
- Exact determination of flaw size using focused probes is possible only if the following conditions exist: 1) the reflector surface is smooth and even; 2) the acoustical axis of the sound beam is vertical to the surface of the reflector; 3) the effect of an ideal reflector on an ideal sound field is considered correctly. An interpretation of the previous confirms the opinion that it is doubtful whether the optimized techniques used for ideal reflectors lead to satisfactory results for natural reflectors, regardless of phase relationships (6.2.3).
- Focusing is advantageous for small reflectors plus being less sensitive to variations in flaw slant angles; there is no advantage with large defects (7.5.3.2).
- Focused probes are superior for detecting poorly oriented flaws under static conditions; however, this superiority is markedly reduced for the moving probe case (4.6.4).
- Acoustic holography and focused probes offer the best possibilities for sizing real flaws (7.2.1).

15.2.12 UT Techniques

- Rayleigh wave examination of fatigue cracks tends to lead to under-estimation of crack depths if there is premature mode conversion from crack-face irregularities (5.5.5).
- While Rayleigh waves may size detected defects well, they often do not detect because of transparency effect or severe attenuation on rough crack surfaces (5.5.5).
- A multiple-beam technique has substantial promise for detection of cracks under cladding because of the ability to optimize beam conditions (6.5).
- The multiple-beam techniques represents a special case of T-R probes where angle, frequency and wave form can be optimized. Not too surprisingly it is successful in detection and sizing of flaws (7.6.3).
- T-R probes are poorer than single probes at longer ranges because of exaggerated increases in pulse length (7.5.3.4).
- T-R probes have poor sensitivity (as do single probes) close to the surface. This is due to beam shape (7.5.3.4).
- The tandem technique affords added reliability in evaluating defects compared to the single-probe technique (2.3).
- Small planar defects normal to the surface are difficult to recognize from either side because most of the UT beam is reflected in different directions. The tandem technique may help (2.3).
- Dual-beam probes are best for skewed reflectors; otherwise they are disadvantageous (7.5.3.2).
- EMATS provide a technique for generating horizontally polarized shear waves for examination with no side effects because there will be no mode conversion (7.6.3).
- Creeping waves offer promise for detection of near-surface cracks, particularly under cladding (7.6.5).

15.3.1 Operator

- The operator is an important link. Under the same conditions with the same equipment, one will correctly define a flaw while the other will call it a geometric reflector (7.4).
- Pronounced variability was observed among various operators in their ability to detect and size flaws in PVRC 251 block (3.2.4.3).
- The human variable is quite marked. Some teams achieved acceptable overall reliabilities of detection for a given series of examinations whereas other teams using similar equipment or the same procedures had poor reliabilities. This means composite results do not portray the correct picture because the distribution is bimodal (3.6).
- Training of UT personnel on piping containing defects such as IGSCC should be productive (4.5.2.3.4).
- The human variable should be minimized by automation, special training, etc. (4.7).
- Both instrument and operator error are significant factors in sizing and location of flaws even more so than in detection (5.7).
- Pulse-echo examinations can vary widely because of the degree of subjectivity in the evaluations. In some instances, excellent results are obtained; in others, results are poor. If decisions are based on amplitude signal alone, great care must be exercised to ensure that the optimum signal is developed, and operators with appropriate field experience are a critical link. It is recognized that other techniques such as tandem provide an alternate (7.4.1).

15.3.2 Coupling

- Errors in technique or procedure in terms of dB are coupling factors (± 2.0) and operator variables (± 2.0) (5.3.1.1).
- Control of coupling gap to less than 0.2 mm is desirable (4.5.2.3.4).

15.3.3 Calibration

- Both PVRC and PISC confirm that inattention to calibration procedures will invalidate results on specimens (7.4).
- Gain adjustment using calibration blocks does not eliminate response variation caused by variation in frequency or search unit size. This is because with most calibration blocks, the energy impinges on the reference hole at normal incidence so the effect of variations in beam profiles does not become apparent (4.5.1.1).
- Reference block containing real defects rather than drilled holes are advantageous (4.7).
- Calibration against notches (ASME XI 1977 Ed.) is preferred to side-drilled holes (1974 Ed.) because the notches more closely resemble cracks and have lower sensitivity (2.3.3, 4.5.1.3).
- Problems arising from calibration are a direct result of using amplitude as a means of detecting and sizing defects. The use of flat-bottomed or side-drilled holes is misleading in that there is no good correlation with real defects (4.5.1.3).
- The acoustic properties of ferritic calibration blocks may vary markedly, particularly if the blocks contain inclusions (4.5.1.5).
- Austenitic calibration blocks can vary markedly in UT test response and in apparent shear-wave beam angle from block to block and block to specimen (4.5.1.4).

15.3.4 Sizing

- Reliability of sizing has been oversold for state-of-the-art UT; therefore, it is prudent to iterate between two or more independent systems (5.5.5).
- The sizing techniques used--half-height, 6-dB drop, 10-dB drop, 20-dB drop, vanishing echo, etc.--yield different results which in turn are dependent on the corrections or lack of corrections made with regard to beam spread (5.7).

- A conclusive determination of defect size by use of 6- to 20-dB drop methods is impossible if the lateral dimensions of the defects are smaller than the sound-field diameter (7.2.1).

15.3.5 Miscellany

- Correct location of defects was poor. Teams consistently did not relate dimensions to the benchmarks or did not read dimensions correctly (3.5.4, 3.6.2).
- Scanning motion may yield a higher echo amplitude than a stationary probe. The two values converge with increased beam divergence (4.6.4).
- The change from 20% DAC reporting level (1974 ASME XI Code) to 50% recording and 100% reporting level in 1977 Edition markedly reduced flaw detection reliability (2.3.3).
- The new PVRC-UT procedure, which is essentially ASME XI Appendix I, was substantially better than the old more permissive procedure (3.2.4.3).
- With IGSCC detection may be quite good; however, correct interpretation ranges from fair to poor. The same applies to fabrication defects. False calls also are a problem (4.5.2.3.4).
- A need exists to improve the methods for detecting off-axis flaws (skewing) either with improvements in technique or in scanning procedure (4.5.2.3.4).

15.3.6 Distance-Gain-Size

- If echo amplitude comparison techniques like DGS are used, then variations in compressive stress or crack roughness will lead to appreciable sizing errors. The reduced response may also result in defects being missed entirely if an echo amplitude threshold is used to define recordable defects (4.6.3).

- Correctable sources of error in sizing with DGS are effective probe diameter and frequency. The error in the first may be as large as 15%; with frequency a similar error plus or minus may occur (5.2.4.1).
- DGS used to size flaws in forging resulted in consistent underestimation of size in terms of both length and area. This also occurred with small flaws (5.6.2.4).
- The AVG (DGS) technique may yield substantial errors in flaw sizing. For example, for surface flaws there is a pronounced magnification effect, as the distance from transducer to flaw increases the size will be overestimated and variability in pulse strength will lead to overestimation for distant reflectors (4.6.2).
- Comparisons of true flaw sizes with those predicted by DGS indicates consistent underestimation (5.2.4.1).
- The DGS method will size circular reflectors but not actual flaws (7.2.1).

15.3.7 UT Techniques

- The significance of attenuation difference unless accounted for can be substantial. For example, if the attenuation is greater than 20 dB between standard and weld, the weld will not be adequately examined (4.5.2.3.2).
- Crack-tip echoes are small; typically they are 50 dB down on a back-wall echo at the same range when using a compression wave probe at grazing incidence, and they are practically impossible to identify if the cracks are in compression or if the material contains other defects such as inclusions (4.6.3).
- An optimized scattered amplitude technique requires careful selection of frequency, pulse length and electronic characteristics so that grain boundary echoes are largely in phase; under these conditions the observed signal is a measure of the presence of cracks (6.2.4).

- In crack-tip diffraction, several mechanisms contribute to the signal including the near-surface wave and a composite pulse arising from grain-boundary scatter. This means there may be marked variations from component to component (6.2.8).
- Flaw-tip reflection avoids the assumption of maximum amplitude of signal at the center of a flaw and estimates flaw size better than the dB-drop techniques (5.6.2.5).
- The errors in sizing cracks in clean material by detecting the tip echoes are typically ± 1 mm if averaged for several probes. Individual readings, however, may be in error by several millimeters. When testing from the crack-breaking surface, the values of crack depth are usually slightly less than those recorded when testing from the opposite surface (4.6.3).
- Spatial averaging may not be effective in moving probe situations because the signal is averaged out as well as the interference due to probe movement (6.5).
- Amplitude-dependent, threshold-detection methods are most reliable when used in conjunction with pattern-recognition methods that do not depend on signal amplitude per se (6.5).
- Both time delay and amplitude variation have been used with Rayleigh waves in measuring surface flaws. Both underestimate or overestimate without an obvious correlation with actual crack depth; however, time delay has substantially less error (4.6.4).
- Sizing by time delay is affected by several factors such as 1) length along the crack face rather than depth of penetration is measured; 2) Rayleigh-wave travel times are affected by surface irregularities; 3) shear-wave signals of comparable height may arrive earlier than the surface wave (6.3).
- Another time domain approach is the satellite pulse-technique which is independent of 1) signal parameter from peak amplitude, 2) defect location, and 3) operating frequency (6.3).

- Results in the frequency domain using Adaptive Learning (ALN) has been very successful under certain circumstances; however, it has proven variable under other circumstances (6.17).
- Signal processing (deconvolution) permits an increase in effective bandwidth when examining austenitics (4.5.2.3.4).
- Acoustic holography offers a promising approach for flaw sizing; however, the poor axial resolution is a limitation since this dimension is critical in fracture mechanics (7.7.1).
- Synthetic Aperture Focusing Technique (SAFT-UT) has definite promise in sizing and location of flaws because of its 1) simultaneous high lateral and longitudinal resolution ($\sim 1 \lambda$); 2) high S/N ratio; 3) wide beam-width insonification; 4) wider bandwidth insonification, and 5) inherently quantitative and volumetric attributes. Its depth resolution is markedly superior to holography; a limitation is that it is slow (6.6.3).

15.4.1 Flaw Angle

- An adverse angle resulting from probe angle and flaw orientation may cause a majority of the UT beam not being reflected back to the probe. UT from both sides of the weld helps (2.3).
- With surface flaws using Rayleigh waves deviation from orientations normal to the surface may result in overestimation or underestimation of flaw depth (4.6.4).
- Crack orientation is a significant factor; while skewed cracks are relatively rare it must be recognized that they are very difficult to detect with normal UT procedures; rotating the transducer as suggested in the German HP 5/3 code improves detection (4.5.2.3.4).
- Flaw characteristics such as orientation and dimensions are sensitive to UT conditions. Variations in equipment and technique will influence (either adversely or favorably) the probabilities of flaw detection, sizing and location (4.5.2.4).
- Flaws oriented at angles substantially away from the normal to the surface are difficult to detect with conventional UT (3.6).
- Flaw orientation is a critical factor. Flaw tilt of $\sim 10^\circ$ is sufficient to prevent detections with a given beam angle. Another factor is due to the UT beam traveling at too flat an angle in relation to the defect so that there is insufficient reflection (4.6.4).
- The significance of flaw surface condition and flaw tilt for a given beam angle helps explain the missed flaws when examinations are limited to one surface and one or two beam angles (4.5).
- Oblique incident beams may lead to errors in analyses because smooth cracks backscatter only weak edge waves which may be mistaken for slag lines, and rough cracks may have an undulating echo similar to that of slag cluster (5.3.9).

15.4.2 Multiple Items

- The following are three major factors leading to errors in sizing of defects: 1) roughness of the flaw; 2) inclination of the flaw to the search beam direction; and 3) curving front and back of flaw (5.2.4.1).
- Factors leading to underestimation of crack depth and, possibly, crack length include 1) tight cracks where transmission losses differ from open cracks; 2) specular reflection from smooth surfaces which is often the case for the lowest or latest generated section of a fatigue crack; 3) crack branching; 4) diffraction effects; and 5) premature mode conversion of Rayleigh waves, possibly from irregularities down a fatigue crack face (4.6.1).
- Dominant sources of sizing error with fatigue cracks are defect roughness (± 3.0), orientation (± 3.5) and transparency (± 4.0) with the latter most significant. Estimates of the error in terms of dB are given (5.3.1).

15.4.3 Miscellany

- There is a need for carefully planned and statistically designed experiments to establish sources of error and means of correction for near-field flaws. The effort should include sizing and location by conventional or advanced UT techniques, and confirmation by destructive testing. The tests should cover fairly thick sections of both ferrite (clad or unclad) steel and austenitic steel containing weldments (5.7).
- PISC results indicate that detection probability and sizing are better for defects near the inner surface of the plate, and are worse for defects near the outer surface of the plate and for defects in the central part of the plate (3.5.5).
- Laminations may shadow planar defects (2.3.1).
- The opposite of lack of detection is the large number of geometric reflectors which may outnumber flaws 1000 to 1. These require extensive evaluations (4.5.2.2).

- The presence of liquid in a crack causes a marginal increase in reflection for shear-wave beams incident at 20° to the crack normal. Modest decreases in reflection occur for beams incident at 45° , while considerable decreases are likely at 30° incidence (4.6.3).
- Correlation between real defect size and estimated defect size is lower than one might have reasonably expected for the PVRC plates provided (PISC) (3.5.5).
- UT test response from intergranular stress corrosion cracking can be much lower than the response from artificial calibration reflectors (4.5.1.4).
- For natural reflectors structure is a decisive factor compared to wave length. Steps taken to improve measurement of dimensions include use of wavelengths large in comparison to defect structure, use of a relatively wide sound beam providing it is smaller than the reflector; focused beams are sensitive to flaw structure so that single reflectors are recorded by several indications (6.2.3).

15.4.4 Stress Effects

- Natural flaws provide the best results in interpreting the effects of stress. The results correlate well with actual flaw depths (5.4.2.2).
- Another potential source of error related partially to the flaw and partially to the material is the effect of regions of high stress that may occur in the crack-tip region since they represent centers for acoustic scattering (4.5.2.3.3).
- Artificial flaws subjected to increased applied stress lead to underestimates of depth and overestimates of length. With natural cracks apparent crack depths increase with stress (4.6.3).
- Shrinkage stresses may result in compressive loads on the crack rendering it transparent to the UT beam--with smooth surfaces and low frequencies. Complete transparency is probable at $\geq 20 \text{ N/mm}^2$ compressive stress (2.3.1).

- Although compressive residual stresses may lead to cracks being undersized, fracture mechanics predictions of growth rates may nevertheless be conservative because these stresses reduce the resultant stress intensity at the crack tip (4.6.3).
- The growth conditions of fatigue cracks have a significant effect on their ultrasonic response, both at zero load and when under compressive stress. The stress intensity factor, K , during crack growth correlates well with the roughness of the fatigue crack surfaces and this is believed to cause the changes in ultrasonic response (4.6.3).
- Low-cycle fatigue flaws are more detectable than high-cycle fatigue flaws because the higher stress of low-cycle fatigue leads to lower residual stress and larger crack opening displacement (4.2.4.1).
- Sizing using crack-tip echoes should only be relied upon if the region of the crack tip is known to be unstressed or under tension (4.6.3).
- The precision of the UT technique measured by the standard deviation increases with increasing stress on a flaw (4.2.2.2).
- Both increasing crack roughness and increasing compressive stresses reduce the specular reflection from fatigue cracks. Decreases of up to 10 dB may be caused by crack roughness while large compressive stresses may cause decreases of up to 20 dB. However, the two effects are not cumulative as the roughest cracks show little variation with stress. To some extent it may be possible to compensate for these effects by increasing the test sensitivity, but this will be limited in practice by the number of insignificant inclusions which give ultrasonic echoes similar in amplitude to those from cracks (4.6.3).
- The combined effects of crack closure and crack surface topography make it impossible to develop a usable correlation between UT response from reference notches and fatigue cracks (4.6.3).
- Crack closure of fatigue cracks in the near-tip region significantly affects UT response due to transparency effects (4.6.3).

- UT shear-wave measurements do not measure the true depth of the flaw, but only the depth for which the crack opening displacement is greater than some critical amount; hence, upon stressing the flaw, the apparent indicated crack depth increases. This increase in apparent crack depth indication appears to be a linear function of stress (4.2.2.2).

15.4.5 Fatigue Flaws

- The ultrasonic responses and surface profiles of fatigue cracks should be measured for a wide range of ΔK and R values so that the dependence of the crack roughness on the growth conditions can be firmly established. This should be repeated for several materials with different values of yield stress using both parent plate and weld metal (4.6.3).
- UT beam behavior is sensitive to the flaw size with respect to wavelength ranging from incoherent scattering for small flaws to complete reflection with large ones (5.3.9).
- When designing inspections to detect high-cycle fatigue cracks the orientations of the ultrasonic beams relative to the likely directions of crack growth should be such that the specularly reflected waves are detected. Realistic estimates of crack-growth directions and compressive stresses during testing are necessary if reliable and cost-effective inspections are to be achieved (4.6.3).
- Substantial differences in signal are observed between artificial and fatigue cracks. For example, 10 to 34 dB added gain was required for reliable fatigue crack detection which was attributed to partial transparency of the fatigue crack and diffuse scattering due to surface roughness (4.2.4.1).

15.4.6 Skewing

- Beam skewing is zero at 0° and 90° ; however, beam angles of only a few degrees on either side of these values lead to a large skewing effect. If the angles are 0° to 15° , the wavefront deviation will

be positive and increase with beam angle; therefore, the beam will diverge and S/N ratios will be poor at 75° to 90° . Where the deviation is negative and decreasing, there will be the same (13.4.2.3).

- Attenuation in anisotropic material can be minimized if the angle between the UT beam and the columnar grains in the weld is 45° . This assumes the orientation is uniform (4.5.2.3.4).
- The form of UT waves that will interact minimally with a (coarse-grained) structure should be selected. Preferred choice is L-waves with S-H waves a possibility. This will have less skewing and scattering (13.4.3, 7.5.3).
- UT propagation depends strongly on the orientation of the beam relative to the fiber axis. The beam width varies, being smallest when the sound propagates along a direction of minimum velocity. The S/N ratio is also a maximum when the beam is along such a 45° direction (4.5.2.3.4).
- Several anomalies may occur in austenitic weldments. The weld fiber axis will influence attenuation, cause beam skewing, possibly lead to beam channeling, and greatly enhanced transmission has been reported to occur at the base metal/weld interface with no satisfactory explanation (4.5.2.3.4).
- The location of maximum wave velocity (45°) also as the location of maximum beam convergence. Similarly as velocity decreases the beam diverges (13.4.2.4).

15.4.7 Scattering/Attenuation

- In coarse-grained textured materials scattering is a significant contributor to attenuation with reflection, and refraction and mode conversion contributing to scattering. Both Rayleigh and stochastic scattering can contribute to the overall effect where Rayleigh scattering varies as the third power of grain size and inversely as the fourth power of wave length. Stochastic scattering varies directly with grain size and inversely as the square of wave length (13.4.2.1).

- While convergence and divergence are important contributors to signal amplitude and attenuation, the contribution must be kept in perspective. For example, diffusive scattering in coarse-grained ($D > \lambda$) materials can be quite severe. There can be strong reflections at grain boundaries leading to strong attenuation (13.4.2.4).
- Welding procedures and methods should be recorded and made available for ultrasonic inspection. The direction of ultrasonic attenuation can then be established (4.5.2.4).

15.4.8 Surface

- On an irregular surface UT sensitivity will vary erratically from place to place. On the average, the sensitivity loss depends markedly on the surface texture and little on the probe design. On the best hand-ground surfaces typical losses vary between 2 and 6 dB (4.5.2.1).
- The deleterious effects of poor surface finish are most pronounced with high-angle (e.g., 70°) probes. Single defects can appear as two or more, and errors in through-wall size can be large (4.5.2.1).
- On an uneven surface the 20-dB drop sizing technique will systematically underestimate a defects through-wall extent. The maximum amplitude technique does not suffer this shortcoming, though it is still impaired by component roughness (4.5.2.1).
- Surface roughness will cause beam diffusion when peak-to-valley ratios or $R_a > \lambda/2$ (4.6.4).
- Suitable surface preparation will minimize coupling problems (4.7).
- For reliable UT inspection the probe contact surfaces must be smooth and even (4.5.2.1).

15.4.9 Cladding Effects

- Cladding can lead to either overestimation or underestimation of flaw size, depending on technique used (5.6.2.1).

- The zero-degree, angle-reflection accuracy with cladding is believed to be caused by scattering on the concave surfaces of the weld beads. Since the shear wave reflects back along the initial path, it is defined as zero degree (4.5.2.3.1).
- Cladding is a definite deterrent to flaw detection based on PVRC Plate 201 results (3.2.3).
- Further tests are required to quantify reliability of detection of near-field cracks, particularly through cladding (3.6).
- Examination through cladding grossly reduced reliability of detection compared to examining the same flaws with the same techniques in the absence of cladding (3.6).
- The effects of austenitic stainless steel overlay clad extends beyond attenuation. Angle-beam misdirection and zero-degree angle reflection may result from the unusual grain structure (4.5.2.1).
- In nozzle examinations, cladding may lead to echoes, reflection, refraction and mode conversion—all militating against detection of smaller cracks (5.6.2.7).

15.4.10 Geometric Effects

- Component geometry is a critical factor. A combination of flaw location and orientation in a nozzle may result in no detection when examined from one surface while it is detected when examined from the other surface (4.5.2.2).
- When examining the inner radius of a nozzle from the outer surface, changes in geometry along the circumference of the vessel require a series of beam angles for each crack position. While such a need is recognized there are no specific code requirements (4.5.2.2).
- Convex surfaces (piping nozzles) may cause problems due to limited transducer contact area, controlling both shape and size of transducer (4.6.4).

- In piping the axial position of the counterbore and the angle of taper can cause mode conversion with the shorter counterbore more prone to mode conversion (4.5.2.2).
- There is a real need to modify field welding procedures and weld configurations to improve ISI. Examples include moving the counterbore away from the weld fusion zone, reducing the taper in the counterbore and modifying the rules regarding drop through and suck-up control of weld crown is necessary too (4.5.2.3.4).
- The structure of weld metal depends on the material and more on the welding technique. The features which determine the UT characteristic of the weld metal are not fully understood yet, due to the complexity and interplay of individual factors (4.5.2.4).
- The inspection of austenitic weldments should not be divorced from welding procedures and the link between these procedures should be established in future NDE (4.5.2.4).
- Lack of fusion between weld beads will lead to curved surfaces which cause UT beam scatter (2.3.1).

15.4.11 Austenitic-Special Items

- Plant designers should take into account the limitations of UT inspection when applied to austenitic structures at the earliest possible stage (4.5.2.3.4).
- Codes should be modified to make use of the fitness-for-purpose approach; i.e., to take into account the special properties (toughness) of austenitic steels (4.5.2.4).
- Sensitivity to small defects, in general, will be less high for austenitic welds than for ferritic even when the weld structure has been optimized. Therefore, austenitic welds should be examined to acceptance standards based on fracture considerations (4.5.2.3.4).

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