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A NEW INDUSTRY DOCUMENT DETAILING BEST PRACTICES IN PIPELINE DEFECT ASSESSMENT

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ABSTRACT

Defects can be introduced into pipelines during manufacturing (e.g. laminations), transportation (e.g. fatigue cracking), fabrication (e.g. weld defects) and installation (e.g. dents), and can occur both due to deterioration (e.g. corrosion) and due to external interference (e.g. gouges and dents).

Operators must be able to both detect and assess the significance of pipeline defects, to ensure pipeline integrity. Consequently, the past 40 years has seen the development of 'fitness-for-purpose' methods for assessing the significance of pipeline defects. However, there is no definitive guidance that draws together all of the assessment techniques, or assesses each method against the published test data, or recommends best practice in their application.

A Joint Industry Project is being sponsored by fifteen international oil and gas to develop a Pipeline Defect Assessment Manual (PDAM). PDAM documents the best available techniques for the assessment of pipeline defects in a simple and easy-to-use manual and gives guidance in their use. PDAM is based on an extensive critical review of pipeline 'fitness-for-purpose' methods and published test data.

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This paper describes the project, and summarises some of the best methods currently available for assessing defects in pipelines.

NOMENCLATURE

$2c$	length of part-wall metal loss defect (mm)
d	depth of part-wall metal loss defect (mm)
t	pipe wall thickness (mm)
A	fracture area of a 2/3 Charpy specimen (53.55 mm^2 for a 2/3 Charpy specimen) (mm^2)
C_V	2/3 thickness specimen upper shelf Charpy V-notch impact energy (J)
D	outside diameter of pipe (mm)
E	Young's modulus (207,000 Nmm^{-2})
H	dent depth (mm)
H_o	dent depth measured at zero pressure (mm)
H_r	dent depth measured at pressure (mm)
K_1	non-linear regression parameter
K_2	non-linear regression parameter
R	outside radius of pipe (mm)
$\bar{s}$	flow stress
s_q	hoop stress at failure (Nmm^{-2})
s_Y	yield strength (Nmm^{-2})
s_U	ultimate tensile strength (Nmm^{-2})

1 INTRODUCTION

Oil and gas transmission pipelines have a good safety record. This is due to a combination of good design, materials and operating practices. However, like any engineering structure, pipelines do occasionally fail. The major causes of pipeline failures around the world are external interference and corrosion; therefore, assessment methods are needed to determine the severity of such defects when they are detected in pipelines.

Defects occurring during the fabrication of a pipeline are usually assessed against recognised and proven quality control (workmanship) limits. These workmanship limits are somewhat arbitrary, but they have been proven over time. However, a pipeline will invariably contain larger defects at some stage during its life, and these will require an engineering assessment to determine whether or not to repair the pipeline. This assessment can be based on 'fitness for purpose', i.e. a failure condition will not be reached during the operation life of the pipeline.

The failure of defects in pipelines has been the subject of considerable study over the past 40 odd years, with a large number of full scale tests, analyses and other work having been undertaken. There are many different fitness-for-purpose methods available for assessing defects in pipelines. Some of these methods have been incorporated into industry guidance, others are to be found in the published literature. Many of the methods are semi-empirical, and therefore limited by the extent of the relevant test data. However, there is no single document that encompasses the current state of knowledge of fitness-for-purpose methods for assessing defects in pipelines, or gives guidance in their use, applicability and limitations.

The Pipeline Defect Assessment Manual is a Joint Industry Project sponsored by fifteen international oil and gas companies (Advantica Technologies, BP, CSM, DNV, EMC, Gaz de France, Health and Safety Executive, MOL, Petrobras, PII, SNAM Rete Gas, Shell Global Solutions, Statoil, Toho Gas and TotalFinaElf) with the intention of developing a Pipeline Defect Assessment Manual (PDAM). PDAM documents the 'best' available techniques for the assessment of pipeline defects in a simple and easy-to-use manual and gives guidance in their use. It is intended to be another tool to help pipeline engineers maintain the historically high level of pipeline safety, and therefore safety is its main goal. PDAM will be completed in December 2001.

This paper summarises some of the methodology and contents of the Pipeline Defect Assessment Manual (PDAM). Best practice for assessing external interference (dents and gouges) is described.

2 FITNESS FOR PURPOSE, ENGINEERING CRITICAL ASSESSMENTS (ECAs) AND PIPELINES

The fitness-for-purpose of a pipeline containing a defect may be determined by a variety of methods ranging from previous relevant experience, to model testing, to 'engineering critical assessments', where a defect is appraised analytically, taking into account its environment and loadings.

2.1 Generic

There are various technical procedures available for assessing the significance of defects in a range of structures. These methods use fracture mechanics; for example the British Standard BS 7910 : 1999[1] contains detailed engineering critical assessment methods, and can be applied to defects in pipelines. Also, there is API RP 579[2] which has similar methods, but with a bias towards their use in process plant.

2.2 Pipeline-Specific

The above standards are generic; they can be conservative when applied to specific structures such as pipelines. Therefore, the pipeline industry has developed its own fitness-for-purpose methods over the past 40 years (and, indeed, documents such as BS 7910 recommend that such methods be used). However, it should be noted that these pipeline specific methods are usually based on experiments, with limited theoretical validation (i.e. 'semi-empirical'). This means that the methods may become invalid or unreliable if they are applied outside these empirical limits.

The pipeline industry has used their fitness-for-purpose methods to produce generic guidelines for the assessment of defects in pipelines. These methods and guidelines range from the NG-18 equations[3] and the Ductile Flaw Growth Model[4,5] developed by the Battelle Memorial Institute in the USA on behalf of the Pipeline Research Council International (PRCI), to the guidelines for the assessment of girth weld defects[6], mechanical damage[7] and ductile fracture propagation[8] produced by the European Pipeline Research Group (EPRG).

2.3 History of Pipeline Defect Assessment Methods.

i. The Early Days….

Fracture mechanics provides the scientific understanding of the behaviour of defects in structures. The effect of defects on structures was studied as long as the 15th century by Leonardo da Vinci, but prior to 1950, failure reports of engineering structures did not usually consider the presence of cracks. Cracks were considered unacceptable in terms of quality, and there seemed little purpose in emphasising this. Additionally, it was not possible to apply the early fracture mechanics work of Griffith to engineering materials since it was only applicable to perfectly elastic materials, i.e. it was not directly applicable to engineering materials which exhibit plasticity.

ii. The Start….

In the 1950s there was major interest in fracture in the aircraft industry in the USA, particularly in aluminium, and in the 1960s there was an increased interest in fracture in nuclear power plant. This lead to the development of fracture mechanics using various approaches (stress intensity factor (K), J integral and crack tip opening displacement).

The 1950s and 1960s was also a period where the safety of transmission pipelines was of interest, primarily in the USA. Early workers on pipeline defects were faced with problems; pipelines were thin walled, increasingly made of tough materials, and exhibited extensive plasticity before failure. Consequently, the fracture mechanics methods at that time could not reliably be applied to the failure of defective pipelines. To have been able to accurately predict the failure stress of a defect in a pipeline they would have needed:

- quantitative fracture toughness data, including measures of initiation and tearing (they only had DWTT and Charpy V-notch impact energy),
- a measure of constraint (this concept was not quantifiable in the 1960s, other than by testing),
- a predictive model for the fracture of a defect in a thin-walled pipe (they had models developed from the nuclear pressure vessel industry), and
- a model to predict the collapse of a defective cylinder.

iii. The Pioneers….

Workers at the Battelle Memorial Institute in Columbus, Ohio decided to develop methods based on existing fracture mechanics models, but they overcame the above deficiencies in fracture mechanics knowledge by a combination of expert engineering assumptions and calibrating their methods against the results of full scale tests. The resulting methods were therefore 'semi-empirical'.

The workers noted that line pipe with defects tended to fail in a ductile manner, but that two basic distinctions could be made:

i. 'Toughness dependent' – these tests failed at lower stresses (pressures). To predict the failure stress of these tests a measure of the material toughness was required (e.g. critical stress intensity factor, K_c, or an empirical correlation based on upper shelf Charpy impact energy).

ii. Strength (or 'flow stress') dependent – these tests failed at higher stresses. To predict the failure stress of these tests only a measure of the material flow stress was required.

The work at Battelle led to the development of the flow stress dependent and the toughness dependent, through-wall and part-wall NG-18 equations[3]. Figure 1 presents a summary of the early test data and the Battelle failure criteria. This work formed the basis of the development of the assessment methods in ASME B31G and modified B31G (RSTRENG), and is still widely applied today.

The original work and models accommodated the very complex failure process of a defect in a pipeline, involving bulging of the pipe wall, plastic flow, crack initiation and ductile tearing. These pioneering models were safe due to inherently conservative assumptions and verification via testing, but they were limited by their experimental validity range (generally, thin walled (plane stress), lower grade, low yield to tensile ratio line pipe).

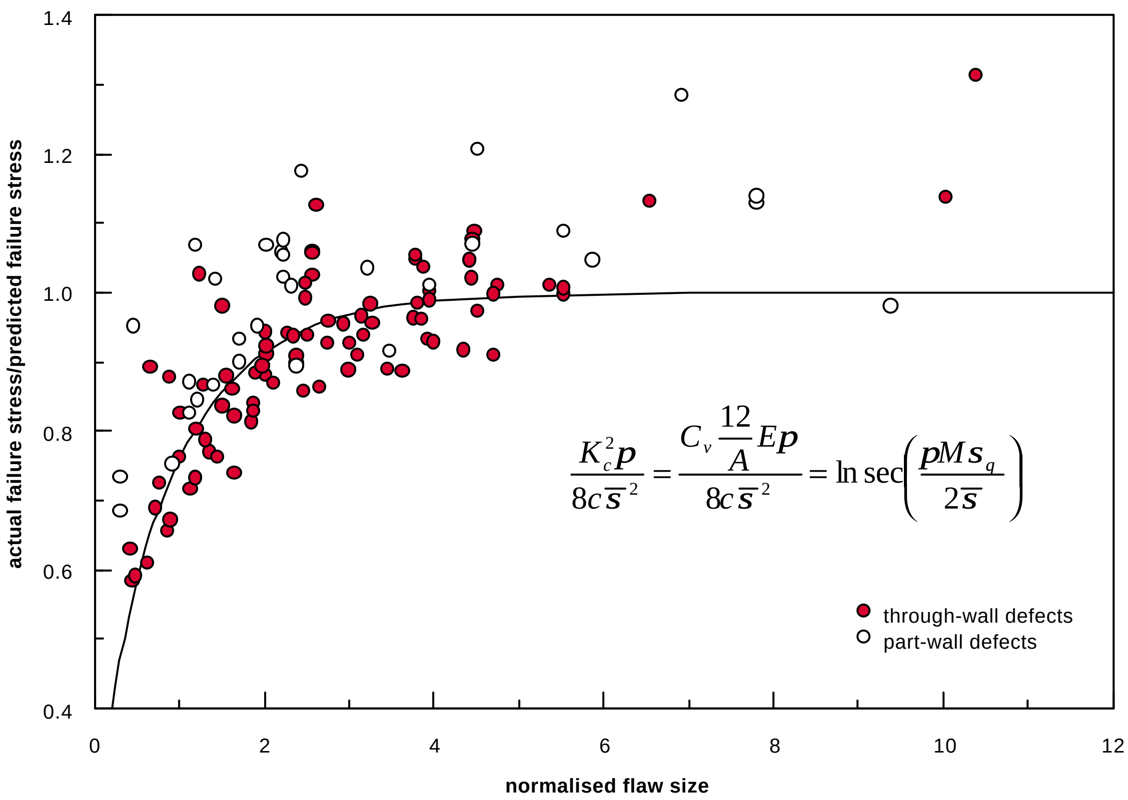


Figure 1 – Summary of the early work at Battelle leading to the development of the NG-18 equations

2.4 Recent Trends in Pipeline Defect Assessment Methods.

More recent work has shown these old methods to still be applicable to many newer pipeline applications, but there has been a heavy reliance on experiments, and more recently numerical analysis. There has been little fundamental work reported, and this is a major, serious and somewhat puzzling omission. It is unreasonable to expect that 30 year old methods will be applicable to newer (e.g. X100 grade) steels, thicker wall (e.g. deep water pipelines approaching 50 mm in thickness), higher strains (deep water and arctic conditions will give rise to greater than 1 percent strains).

The past 10 years has seen a near obsession with proving that these old methods are either:

i. highly conservative, or

ii. applicable to newer materials or applications via simple testing or numerical analysis.

This is the wrong approach; new methods should be developed. The flow stress dependent methods were not conservative (see Figure 1), and will not be theoretically applicable to newer, thicker materials. A more quantitative, explicit understanding of the material toughness is required. Certainly, provided that low constraint and high toughness conditions prevail, experiments will show them to be reasonable (or conservative) in these newer materials and geometries. Derivatives of the older methods that are biased towards the behaviour of modern, high toughness line pipe steels will not be applicable to older line pipe. Without methods that quantify the effects of constraint and toughness, there will be a reliance on experimental validation for all new applications and materials, and empirical constraints.

Accordingly, the PDAM pays close attention to experimental validation, in recognition of the above deficiencies of existing models.

3 THE PIPELINE DEFECT ASSESSMENT MANUAL

PDAM is based upon a comprehensive, critical and authoritative review of available pipeline defect assessment methods. This critical review includes a compilation of all of the published full-scale test data used in the development and validation of existing defect assessment methods. The full-scale test data is used to assess the inherent accuracy of the defect assessment methods, and to identify the ‘best’ methods (considering relevance, accuracy and ease of use) and their range of applicability. PDAM describes the ‘best’ method for assessing a particular type of defect (the types of defect considered in PDAM are described below) defines the necessary input data, give the limitations of the method, and defines an appropriate factor to account for the model uncertainty.

PDAM provides the written text, the methods, recipes for application, acceptance charts and simple examples, and is supported by background literature reviews. Simple electronic workbooks have been developed to permit easy implementation of the ‘best’ methods. The role of PDAM in the fitness-for-purpose assessment of a defect in a pipeline is summarised in Figure 2.

PDAM has been closely scrutinised throughout its development by the sponsors, and all literature reviews and chapters have been reviewed by experts in the field of pipeline defect assessment. Indeed, chapters of PDAM have been reviewed by some of the 'pioneers' of the subject, including John Kiefner and Bob Eiber.

PDAM does not present new defect assessment methods, but rather is based on a critical review of existing assessment methods and test data. PDAM presents the current state of the art in fitness-for-purpose assessment of defective pipelines. Limitations of the methods recommended in PDAM represent limitations of the available methods, and hence of the current state of knowledge.

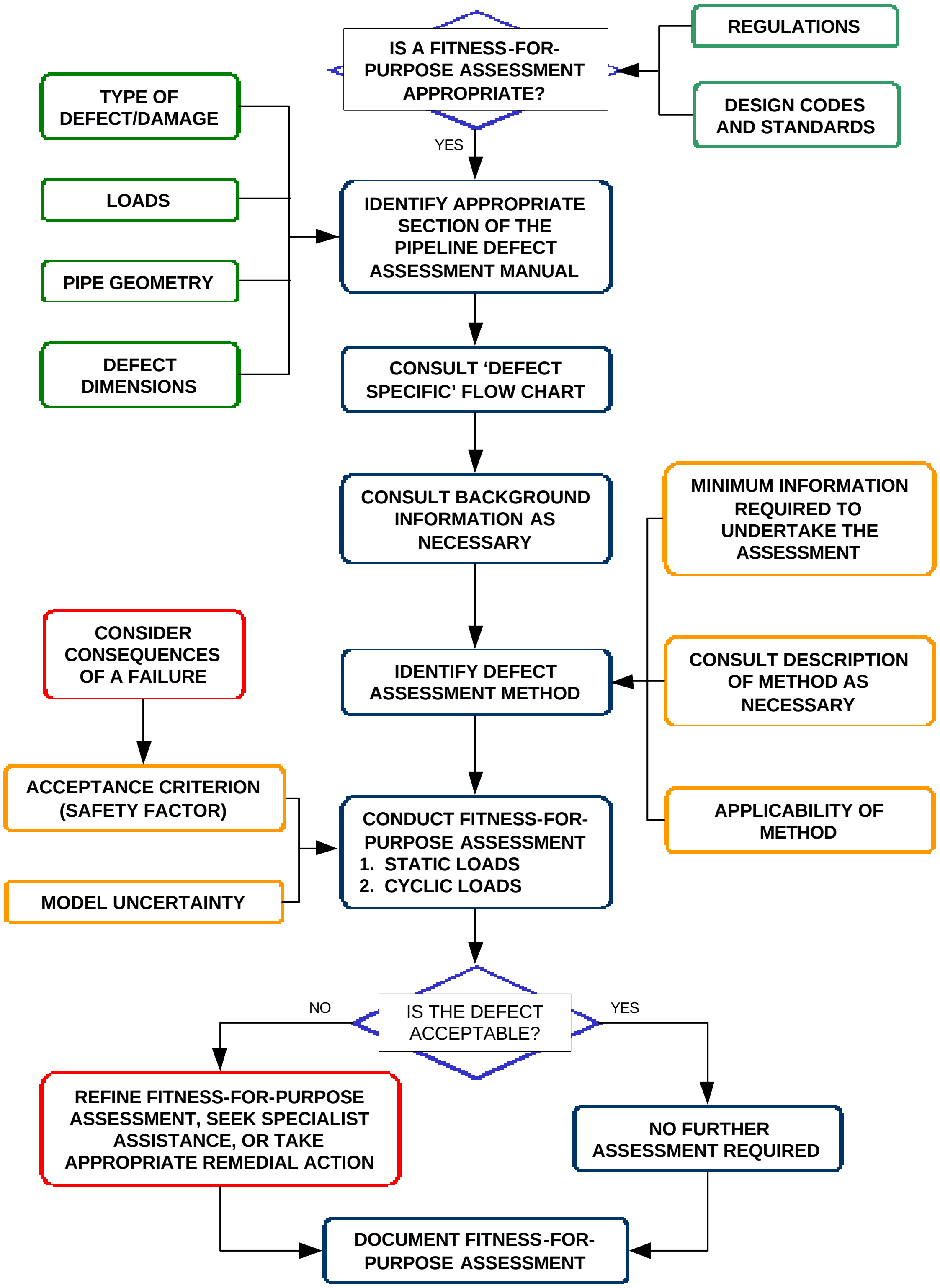


Figure 2 – The fitness-for-purpose assessment of a pipeline defect

4 TYPES OF DEFECT CONSIDERED IN THE PIPELINE DEFECT ASSESSMENT MANUAL

PDAM contains guidance for the assessment of the following types of defect:

- defect-free pipe
- corrosion
- gouges
- plain dents
- kinked dents
- smooth dents on welds
- smooth dents containing gouges
- smooth dents containing other types of defects
- manufacturing defects in the pipe body
- girth weld defects
- seam weld defects
- cracking
- environmental cracking

In addition, guidance is given on the treatment of the interaction between similar defects and different types of defect, and defects in pipe fittings (pipework, fittings, elbows, etc.). Guidance is also given on predicting the behaviour of defects upon failing (i.e. leak or rupture, and fracture propagation).

The following types of loading have been considered in the development of the guidance: internal pressure, external pressure, axial force and bending moment.

Methods are given in PDAM for assessing the burst strength of a defect subject to static loading and for assessing the fatigue strength of a defect subject to cyclic loading. Note that there are some combinations of defect type, orientation and loading for which there are no clearly defined assessment methods.

5 THE FORMAT OF THE PIPELINE DEFECT ASSESSMENT MANUAL

The Pipeline Defect Assessment Manual broadly follows the following format for each defect type and assessment method:

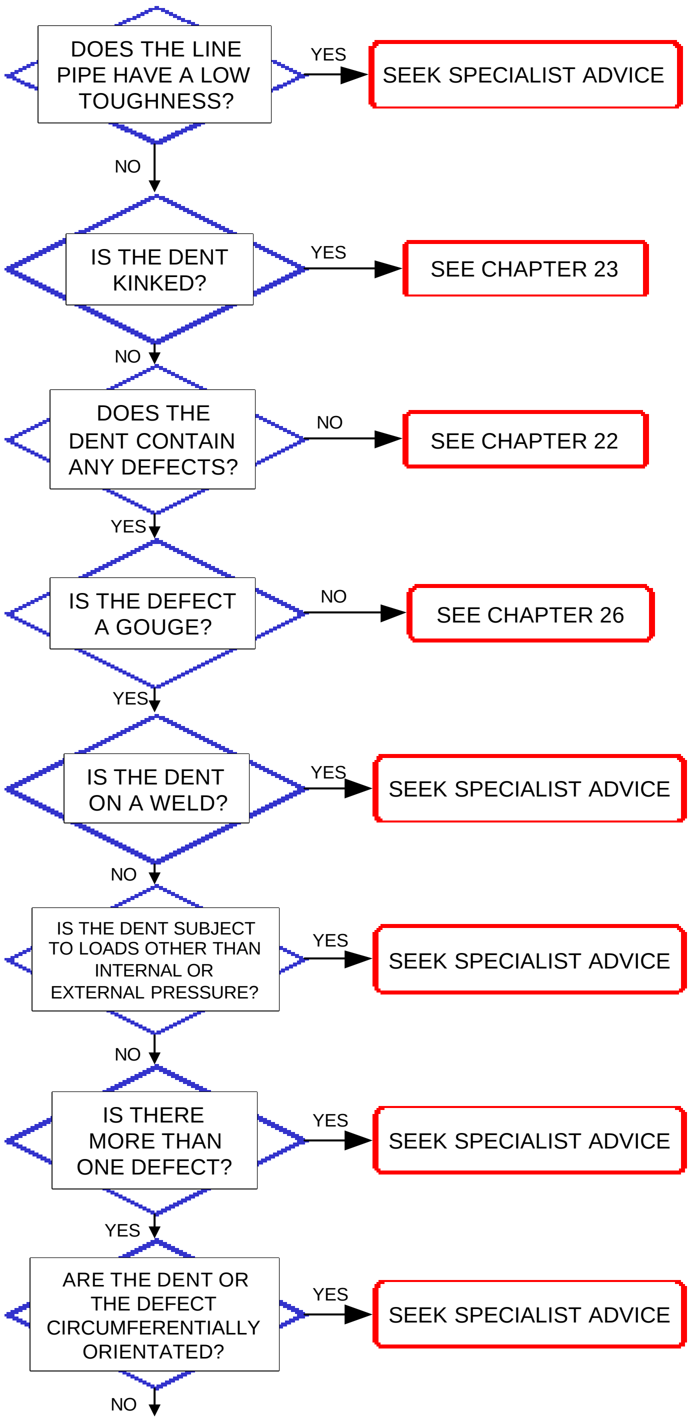
1. A brief definition of the type of defect.
2. A figure illustrating the dimensions and orientation of the defect relative to the axis of the pipe, and a nomenclature.
3. Brief notes that highlight particular problems associated with the defect.
4. A flow chart summarising the assessment of the defect.
5. The minimum required information to assess the defect.
6. The assessment method.
7. The range of applicability of the method, its background, and any specific limitations.
8. An appropriate model uncertainty factor to be applied to the assessment method.
9. An example of the application of the assessment method.
10. Reference is made to alternative sources of guidance available in national or international guidance, codes or standards.

The flow charts included for each defect type generally consist of a number of yes-no type questions designed to identify whether or not the methods contained in that chapter are appropriate to the given case, and to indicate the appropriate method to use. An example of the flow chart for the assessment of a smooth dent containing a gouge in a pipeline is given in Figure 3.

In some cases the flow charts indicate that specialist assistance should be sought. It may be that an assessment would be difficult and time-consuming and, therefore, does not lend itself to the simplified approaches included in PDAM. Alternatively, the particular situation may lie outside the bounds of the assessment methods that are given in PDAM. The literature reviews for each defect type contain detailed background on the methods included in the manual, and other methods in the published literature, that may be of use in such situations.

DENTED PIPELINE

Indications of low toughness include: old linepipe, linepipe not manufactured to API 5L, or an operating temperature less than the DWTT transition temperature.



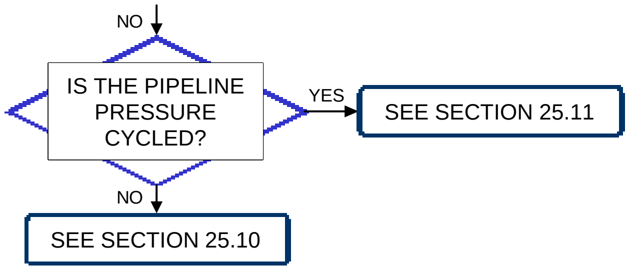


Figure 3 – The Assessment of a Smooth Dent containing a Gouge

6 THE ASSESSMENT OF THE BURST STRENGTH OF A GOUGE

A gouge is surface damage to a pipeline caused by contact with a foreign object that has scrapped (gouged) material out of the pipe, resulting in a metal loss defect. The material at the base of a gouge will have been severely cold worked as a consequence of the gouging process. This work hardened layer will have a reduced ductility and may contain cracking.

A gouge reduces the burst strength and fatigue strength of the pipe.

A gouge may be of any orientation with respect to the pipe axis. A longitudinally orientated gouge is the most severe condition for internal pressure loading, therefore the following discussion concentrates on this orientation.

6.1 Full Scale Burst Tests Of 'Gouges'

There have been a large number of full scale burst tests of longitudinally orientated 'gouges' (part-wall defects) in line pipe steel conducted by a number of different organisations. Other organisations have carried out tests in pressure vessel steels. The total number of published burst tests is of the order of 190, although only the most relevant 115 tests are referred to here. Only in the tests by CANMET were the part-wall defects actually gouges (formed by scraping (gouging) the pipe with a tool bit mounted on a pendulum), the other tests are of machined notches, slots, etc..

Battelle[3] (1965 - 1974)	92 vessel tests (through-wall defects)
Battelle[3] (1965 - 1974)	48 vessel tests (part-wall defects)
British Gas[9] (1974)	3 vessel tests
British Gas[10] (1981, 1982)	1 vessel test
TWI[11] (1982)	2 vessel tests
Battelle[12] (1986)	3 vessel tests
CANMET[13,14] (1988, 1988)	12 vessel tests
CSM-SNAM-EUROPIPE[15] (2000)	2 vessel tests
TÜV and Mannesmann[16] (1987)	15 vessel tests
Herrera et al.[17] (1992)	10 vessel tests
Iron and Steel Institute of Japan[18] (1993)	19 vessel tests

The tests can be variously described as follows:

1. machined 'V-shaped' notch or slot (artificial gouge)
   Battelle (1965 - 1974) (vessels)
   Battelle (1986) (vessels)
   British Gas (1974) (vessels)
   British Gas (1981, 1982) (vessels)
   Iron and Steel Institute of Japan (Kubo et al.) (1993) (vessels)
   CSM SNAM EUROPIPE (2000) (vessels)
2. scrape (gouge) the pipe using a tool bit mounted on a pendulum
   CANMET (1985, 1988) (vessels)
3. fatigue pre-cracked semi-elliptical machined notch
   TWI (Garwood et al.) (1982) (vessels)
   TÜV and Mannesmann (Keller et al.) (1987) (vessels)
   University of Tennessee (Herrera et al.) (1992) (vessels)

A larger degree of scatter is noticeable in the results of tests of fatigue pre-cracked notches, when compared to the tests of machined notches, therefore the tests by Garwood et al. (1982), Keller et al. (1987) and Herrera et al. (1992) are excluded from the subsequent discussion. The tests Kubo et al. (1993) are also be excluded by reason of differences between the test temperature and the temperature at which the material properties were measured.

6.2 Methods for Predicting the Burst Strength of a Gouge

The assessment of the burst strength of part-wall defects in pipelines derives from work conducted at Battelle in the 1960s and 70s, culminating in the development of flow stress dependent and toughness dependent forms of through-wall and part-wall failure criterion, also referred to as the NG-18 equations. The development of the NG-18 equations is described in Kiefner et al. (1973)[3]. The semi-empirical through-wall failure criterion was developed and validated against the results of 92 full scale vessel burst tests containing artificial, longitudinally-orientated, through-wall defects carried out by Battelle. Similarly, the part-wall failure criterion was developed and validated against the results of 48 full scale vessel burst tests containing artificial, longitudinally-orientated, machined V-shaped notches.

Both the through-wall and part-wall criteria are semi-empirical, primarily through the definition of the flow stress; however it is noteworthy that the form of the part-wall criterion is entirely empirical. The flow stress dependent form of the part-wall failure criterion has been widely used as a plastic collapse solution for axial crack-like flaws subject to internal pressure, and appears in documents such as BS 7910 and API 579. Several previously published reviews have concluded that the NG-18 equations are the 'best' equations for assessing part-wall defects such as gouges[19,20]. The part-wall NG-18 equations are also recommended in the EPRG guidelines for the assessment of mechanical damage[7].

The flow stress dependent part-wall NG-18 equations have the following form (note that there are other definitions of the flow stress and the Folias factor (three common definitions are given below)):

$$s_q = \bar{s}\left[\frac{1-\frac{d}{t}}{1-\frac{d}{t}\left(\frac{1}{M}\right)}\right]$$ **Equation 1**

where

$$\bar{s} = \frac{s_Y + s_U}{2}$$ **Equation 2**

$$M = \sqrt{1+0.26\left(\frac{2c}{\sqrt{Rt}}\right)^2} = \sqrt{1+0.52\left(\frac{2c}{\sqrt{Dt}}\right)^2}$$ **Equation 3**

$$M = \sqrt{1+0.314\left(\frac{2c}{\sqrt{Rt}}\right)^2 - 0.00084\left(\frac{2c}{\sqrt{Rt}}\right)^4} = \sqrt{1+0.6275\left(\frac{2c}{\sqrt{Dt}}\right)^2 - 0.003375\left(\frac{2c}{\sqrt{Dt}}\right)^4}$$ **Equation 4**

$$M = \sqrt{1+0.40\left(\frac{2c}{\sqrt{Rt}}\right)^2} = \sqrt{1+0.80\left(\frac{2c}{\sqrt{Dt}}\right)^2}$$ **Equation 5**

The NG-18 equations do not explicitly consider the effects of ductile tearing on the failure of through-wall and part-wall defects. These effects are implicit in the definition of the flow stress (and in the toughness dependent forms, in the correlation between the upper shelf Charpy impact energy and facture toughness). A more sophisticated, but complex, method for assessing part-wall defects, such as gouges, is the Pipeline Axial Flaw Failure Criteria (PAFFC)[5], based on the Ductile Flaw Growth Model (DFGM)[4] developed by Battelle.

6.3 Comparison with Test Data

The flow stress dependent form of the part-wall NG-18 equations developed by Battelle is the 'best' method in terms of the quality of fit with the published test data for predicting the burst strength of a gouge. However, this equation has been published with different definitions of the flow stress and the Folias factor (*M*). The various forms of the NG-18 equations have been compared using the published test data.

Only tests on machined notches have been considered. Tests where there is insufficient data and where the upper shelf 2/3 thickness size Charpy impact energy is less than 21 J (see below) have been excluded. The total number of full scale tests considered in the comparison is 71. The statistics of the ratio of the actual failure stress to the predicted failure stress for the various forms of the NG-18 equations are given in Table 1.

		mean	standard deviation	coefficient of variation	number of tests
(1)	two term Folias (eqn. 5)	1.06	0.16	0.15	71
	three term Folias (eqn. 4)	1.02	0.14	0.14	71
	approximate two term Folias (eqn. 3)	**0.99**	**0.13**	**0.13**	71
(2)	two term Folias	1.05	0.15	0.15	71
	three term Folias	1.01	0.13	0.13	71
	approximate two term Folias	0.98	0.12	0.13	71
(3)	two term Folias	0.95	0.15	0.16	71
	three term Folias	0.92	0.14	0.15	71
	approximate two term Folias	0.89	0.13	0.14	71

Note : (1) average of yield strength and tensile strength, (2) yield strength plus 10 ksi, and (3) tensile strength

Table 1 – Statistical comparison of NG-18 equation with several forms of the Folias factor and flow stress

It is apparent that there is little difference between the three forms of the Folias factor, the approximate two term factor (equation 3) and the three term factor (equation 4) being almost identical. There is also little difference between a flow stress of the average of the yield and tensile strength, and one of the yield strength plus 10 ksi (as quoted in Kiefner et al. (1973)). A flow stress equal to the tensile strength gives, on average, non-conservative predictions, and a slight increase in the scatter. A comparison between the predictions made using the NG-18 equations with a flow stress of the average of the yield and tensile strength and the two term Folias factor (equations 1 to 3), and the published full scale test data is shown in Figure 4.

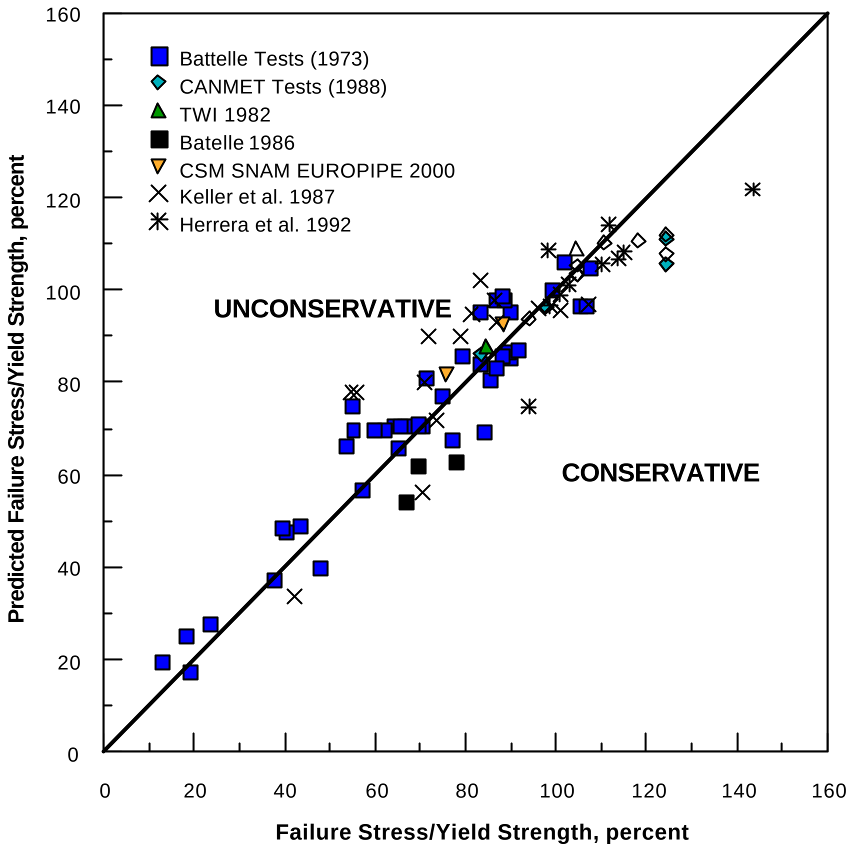


Figure 4 – Failure stress of axially orientated part-wall defects predicted using the part-wall NG-18 equations

6.4 The Effect of Toughness on the Burst Strength of a Gouge

i. Hard Zone Below Gouge

The changes to the local microstructure at the base of a gouge, as a consequence of the gouging process, has been appraised by CANMET in Canada. Their tests indicated that the effect of changes to the microstructure in the base of a gouge were not significant if the upper shelf Charpy V-notch impact energy (2/3 specimen size) exceeded 20 J[28].

ii. Effect of Toughness on the Burst Strength

The burst strength of a gouge (or part-wall defect) is affected by the toughness of the line pipe steel. This toughness dependency is expressed semi-empirically in the toughness-dependent forms of the through-wall and part-wall NG-18 equations (see Figure 1). As the toughness increases the burst strength tends to a toughness independent, or flow stress dependent, form (as in Figure 1 and expressed in equations 1 to 3). Flow stress dependent forms are simpler to use than toughness dependent forms, hence there is an attraction in being able to define a minimum toughness above which the flow stress dependent form of a failure criterion can be applied.

The effect of toughness on the accuracy of predictions of the burst strength of a part-wall defect made with the flow stress dependent part-wall NG-18 equations is illustrated in Figure 5. A flow stress of the average of the yield and tensile strength and a two term Folias factor has been used (equations 1 to 3). It is clear that the predictions become increasingly non-conservative at a lower toughness. The scatter in the range from 20 J to 45 J is also clear, with some tests being non-conservatively predicted and others being conservatively predicted, in an approximate range from 0.80 to 1.20 (ratio of the actual to predicted failure stress). Taking into account the observed scatter, it is reasonable to apply the flow stress dependent part-wall NG-18 equation if the 2/3 thickness specimen size upper shelf Charpy V-notch impact energy is at least 21 J (16 ftlbf). Wall thickness has an effect on toughness dependent failure, increasing wall thickness is associated with increasing constraint. The maximum wall thickness in this set of test data is 21.7 mm. Therefore, this minimum toughness requirement is only valid for line pipe of a thickness less than 21.7 mm.

This toughness limit should not be interpreted as implying that the failure of a sharp part-wall defect (such as a gouge) in line pipe steel with a toughness of 21 J or above is due to plastic collapse (plastic flow). Rather the limit is empirical, and based on the observation that a reasonable mean prediction of the burst strength can be made using the stated flow stress dependent criterion for tougher steels. Clearly this toughness limit is dependent on the inherent conservatism of the stated flow stress dependent criterion. From the trend of failure stress with toughness in Figure 5, it is apparent that a flow stress based on the ultimate tensile strength may be appropriate for high toughness steels (although 'high toughness' remains to be defined).

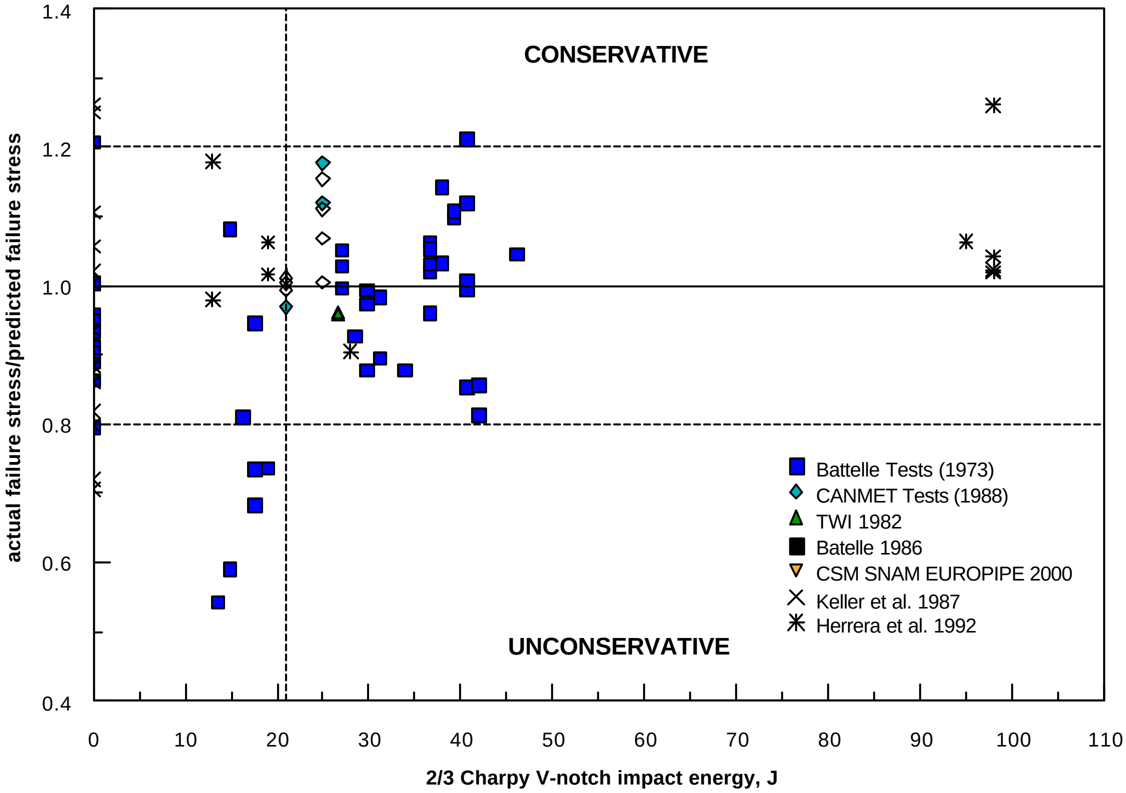


Figure 5 – The effect of toughness on predictions of part-wall burst tests made using the flow stress dependent part-wall NG-18 equations

A similar analysis of burst tests of axially orientated machined slits (through-wall defects) in line pipe indicates that a minimum 2/3 thickness specimen size upper shelf Charpy V-notch impact energy of 40 J (29.5 ftlbf) is necessary for the flow stress dependent through-wall NG-18 failure criterion to be applied. This difference between part-wall and through-wall defects follows the same trend as tests that have indicated that the fracture initiation transition temperature (FITT) (the temperature at which a fracture changes from brittle to ductile) of a part-wall defect is lower than that of a through-wall defect[21].

6.5 Recommendation in the Pipeline Defect Assessment Manual

The Pipeline Defect Assessment Manual recommends the semi-empirical NG-18 part-wall flow stress dependent failure criterion with the approximate two term Folias factor and a flow stress of the average of yield strength and tensile strength (equations 1 to 3). A flow stress of the average of yield strength and tensile strength is to be preferred, in this context, because it gives more consistent results over the whole range of line pipe steel grades.

The equations should not be applied if the 2/3 thickness specimen size upper shelf Charpy V-notch impact energy is less than 21 J (16 ftlbf). The wall thickness must be less than 21.7 mm.

A 'model uncertainty' has been derived from the prediction interval for the classical least squares linear regression model, and this lower bound expression is given in PDAM. The effect of applying a confidence interval corresponding to a 95 percent one-tail confidence level is illustrated in Figure 6; the tests with a toughness greater than 21 J are conservatively predicted.

It is recommended that the measured depth of a gouge be increased by 0.5 mm to account for the possibility of cracking at the base of the gouge, unless an inspection technique is used to detect and measure cracking at the base of the gouge (note that measuring the depth of cracking in a gouge may be difficult because of the morphology of the damage).

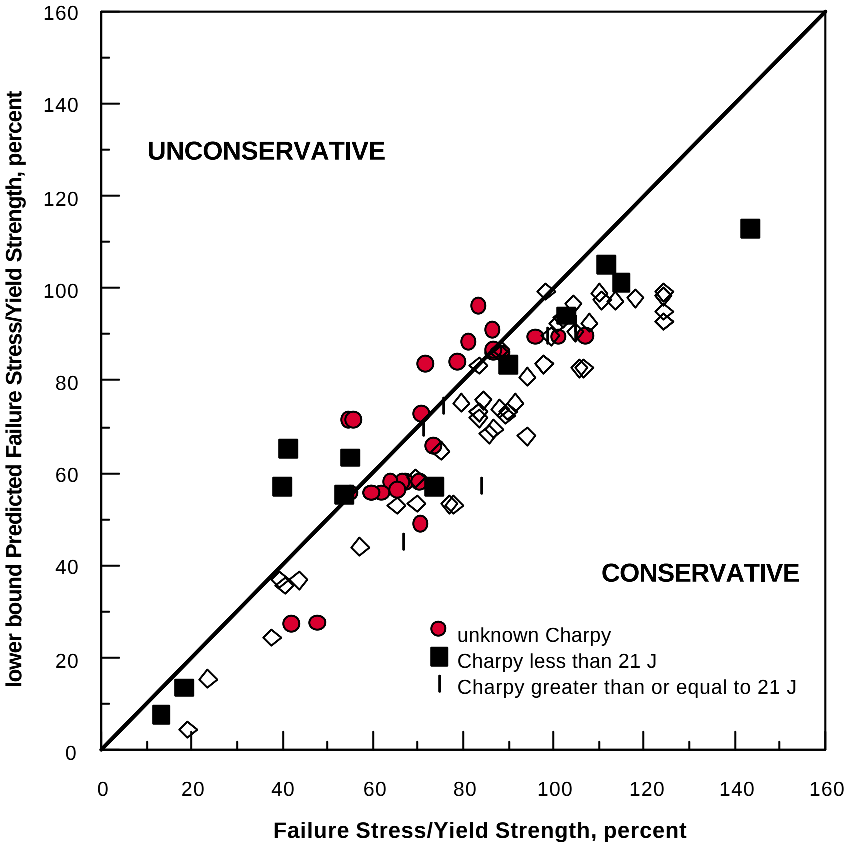


Figure 6 – Failure stress of axially orientated part-wall defects predicted using a lower bound to the part-wall NG-18 equations

6.6 Range of Applicability

The recommended method for assessing the burst strength of a longitudinally orientated gouge has been compared against the results of 92 full scale burst tests of vessels containing artificial, machined part-wall defects and gouges, including some materials other than line pipe steel. The range of the test data included in the comparison is as follows (in SI units). This gives an indication of the range of applicability of the part-wall NG-18 equations.

Pipe Diameter, mm	114.0	to	1422.4
Wall Thickness, mm	5.6	to	21.7
2*R*/*t* ratio	13.3	to	104.0
Grade (API 5L)	X52	to	X100
Yield strength, Nmm^{-2}	379.2	to	878.0

Tensile strength, Nmm^{-2}	483.3	to	990.0
yield to tensile ratio	0.69	to	0.99
2/3 Charpy Impact Energy, J	13.6	to	261.0
Notch Depth (d), mm	0.49	to	16.8
d/t	0.088	to	0.92
Notch Length ($2c$), mm	14.0	to	609.6
$2c/(Rt)^{0.5}$	0.41	to	8.16
Burst Pressure, Nmm^{-2}	1.84	to	142.0
Burst Stress, Nmm^{-2}	61.4	to	880.7
Burst Stress (percent SMYS)	13.7	to	132.5

7 DENT AND GOUGE

A dent is a depression which produces a gross disturbance in the curvature of the pipe wall, caused by contact with a foreign body resulting in plastic deformation of the pipe wall. External interference can cause both metal loss defects (gouging) and dents.

A dent containing a gouge (or other types of metal loss defect) is a very severe form of damage. The burst strength of a smooth dent containing a gouge is lower than the burst strength of an equivalent plain dent, and lower than that of an equivalent gouge in undented pipe. The fatigue strength of a smooth dent containing a gouge is lower than that of an equivalent plain dent because of the additional stress concentration due to the presence of the gouge, and the possibility of local cracking at the base of the gouge.

7.1 Full Scale Burst Tests of Dents and 'Gouges'

A large number of full scale ring and vessel burst tests of a smooth dent containing a single 'gouge' have been conducted by a variety of different organisations, see below. The total number of published tests is 242. However, most of the tests have actually been of machined notches or slots, rather than gouges.

All of the machined notches (slots) and gouges have been longitudinally orientated. All of the dents have been longitudinally orientated, except for the Gasunie tests in which transverse dents were introduced into pipe.

Battelle[22] (1978)	2 vessel tests
Battelle[22-24,12] (1978, 1979, 1986)	30 vessel tests
Battelle[12,25] (1986)	4 vessel tests
British Gas[10] (1982)	100 ring tests
British Gas[10] (1982)	23 vessel tests
British Gas[26] (1983)	1 dynamic vessel tests
Det Norske Veritas[27] (1982, 2000)	9 vessel tests
CANMET[14,28] (1985, 1988)	11 vessel tests
Battelle[12,25] (1986)	17 dynamic[2] vessel tests

[2] The dent and gouge were introduced simultaneously under dynamic loading conditions (as opposed to quasi-static) in order to simulate damage caused by excavation equipment.

Gasunie[29,30] (1986, 1990)	10 vessel tests
British Gas[31] (1989)	8 ring tests
EPRG[32,33] (1991, 1992)	8 vessel tests
Nanyang Technical University[34] (1992)	17 vessel tests
Stress Engineering Services[35,36] (1996)	14 vessel tests
Stress Engineering Services[37] (1997)	3 vessel tests
Tokyo Gas[38] (1998, 1999)	3 vessel tests
University of Cambridge[39] (1992)	9 small scale model tests
University of Cambridge[40] (1993)	15 small scale model tests
University of Cambridge[41] (1996)	20 small scale model tests

Any discussion of the various dent and 'gouge' tests is complicated by the fact that the tests have been conducted by a large number of different organisations and using a variety of different test methods. The damage has been variously introduced at pressure and at zero pressure, the dent before the gouge, the gouge before the dent, and the dent and gouge simultaneously.

A summary of the various types of tests is given below:

1. damage introduced at zero pressure; introduce the dent and then machine a 'V-shaped' notch (artificial gouge) in the base of the dent

   British Gas (1981, 1982, 1989) (ring and vessel tests)

   Tokyo Gas (1998) (vessels)

2. damage introduced at zero pressure; machine a 'V-shaped' notch (artificial gouge) and then introduce the dent

   Battelle (1979, 1986) (vessels)

   Nanyang Technical University (1992) (vessels)

   University of Cambridge (1992, 1996) (vessels)

3. damage introduced at zero pressure; machine a 'V-shaped' notch (artificial gouge) and then introduce the dent (a sharp steel triangle was inserted in the notch between the cylindrical indenter and the pipe)

   DNV (1982, 2000) (vessels)

4. damage introduced at zero pressure; introduce the dent and then scrape (gouge) the pipe using a tool bit mounted on a pendulum

   CANMET (1985, 1988) (vessels)

5. damage (dent) introduced at pressure; machine a ‘V-shaped’ notch (artificial gouge) at zero pressure and then introduce the dent at pressure

   SES (1996) (vessels)

6. damage (dent) introduced at pressure; gouge at zero pressure and then introduce the dent at pressure

   EPRG (1991, 1992) (vessels)

7. damage introduced at a low pressure (150 psi) or zero pressure; damage introduced using an indenter with a machined sharp edge (with a 60 degree included angle) along its length

   Battelle (1978) (vessels)

8. damage introduced at pressure; dent and gouge introduced simultaneously using a specially designed test rig

   British Gas (1983) (vessel)

   Battelle (1986) (vessels)

9. damage (transverse dent) introduced at pressure and gouge introduced at zero pressure; dent at pressure, depressurise (holding indenter in place) and then scrape (gouge) the pipe using the indenter

   Gasunie (1986, 1990) (vessels)

10. damage introduced at pressure; machine a blunt (rounded) notch at zero pressure and then introduce the dent at pressure

    University of Cambridge (1996) (vessels)

11. damage introduced at zero pressure; machine a 1 in. wide slot (artificial corrosion) and then introduce the dent

    SES (1997) (vessels)

Internal pressure stiffens the response of the pipe to indentation, such that dents introduced at pressure will be smaller than those introduced at zero pressure, and puncture is more likely (if the indenter is sharp). Introducing dents at zero pressure allows deeper dents to be formed than would be observed in practice[12]. A ring test simulates an infinitely long ‘gouge’ in a continuous dent. A continuous dent will spring back and reround more than a short dent because it is geometrically less stiff (there is no constraint from the ends of the dent).

7.2 Methods for Predicting the Burst Strength of a Dent And Gouge

The failure behaviour of a dent containing a gouge is complex. A dent and gouge is a geometrically unstable structure. Outward movement of the dent promotes initiation and growth of cracking in the base of the gouge, changing the compliance of the dent. The failure of a dent and gouge defect involves high plastic strains, wall thinning, movement of the dent, crack initiation, ductile tearing and plastic flow. An analysis of the failure mechanism of a dent and gouge defect is described by Leis et al. (2000)[42].

Empirical relationships for predicting the burst strength of a smooth dent containing a gouge have been proposed by British Gas[10,26], the EPRG[7] and Battelle[12,23]. A semi-empirical fracture model for assessing the burst strength of a dent-gouge defect has been developed by British Gas[43], and has subsequently been included in the EPRG recommendations for the assessment of mechanical damage[7].

Note that more sophisticated models are under developed (e.g. Leis et al. (2000)), which attempt to more accurately describe the failure mechanism of a dent and gouge defect.

Probably the two most widely quoted models for predicting the failure stress of a dent and gouge defect that have been developed and documented in the published literature are:

1. The empirical *Q* factor model developed by Battle under the auspices of the Pipeline Research Council International (PRCI)[12,23].
2. The dent-gouge fracture model developed by British Gas and adopted by the EPRG[7,43].

7.2.1 The Empirical *Q* Factor Model

The empirical model for predicting the burst strength of a smooth dent containing a gouge developed by Battelle model was based on the results of 30 full scale burst tests carried out by Battelle[12,22-24], in which the damage was introduced at zero pressure by notching and then denting. An empirical relationship between the failure stress normalised by the flow stress and an empirical parameter, denoted *Q*, was developed. The *Q* factor is defined as a function of the upper shelf Charpy impact energy (for a 2/3 size specimen), the dent depth (after spring back and measured at zero pressure), the gouge length, and the gouge depth.

The empirical relationship is given by the following equations (in imperial units)

$$\frac{s_f}{\bar{s}} = \frac{(Q-300)^{0.6}}{90}$$ **Equation 6**

where

$$Q = \frac{C_v}{\left(\frac{H}{2R}\right)(2c)\left(\frac{d}{t}\right)}$$ **Equation 7**

$\bar{s} = s_Y + 10000$ psi **Equation 8**

Figure 7 shows a comparison between the predictions made using the empirical *Q* factor model and the published full scale test data.

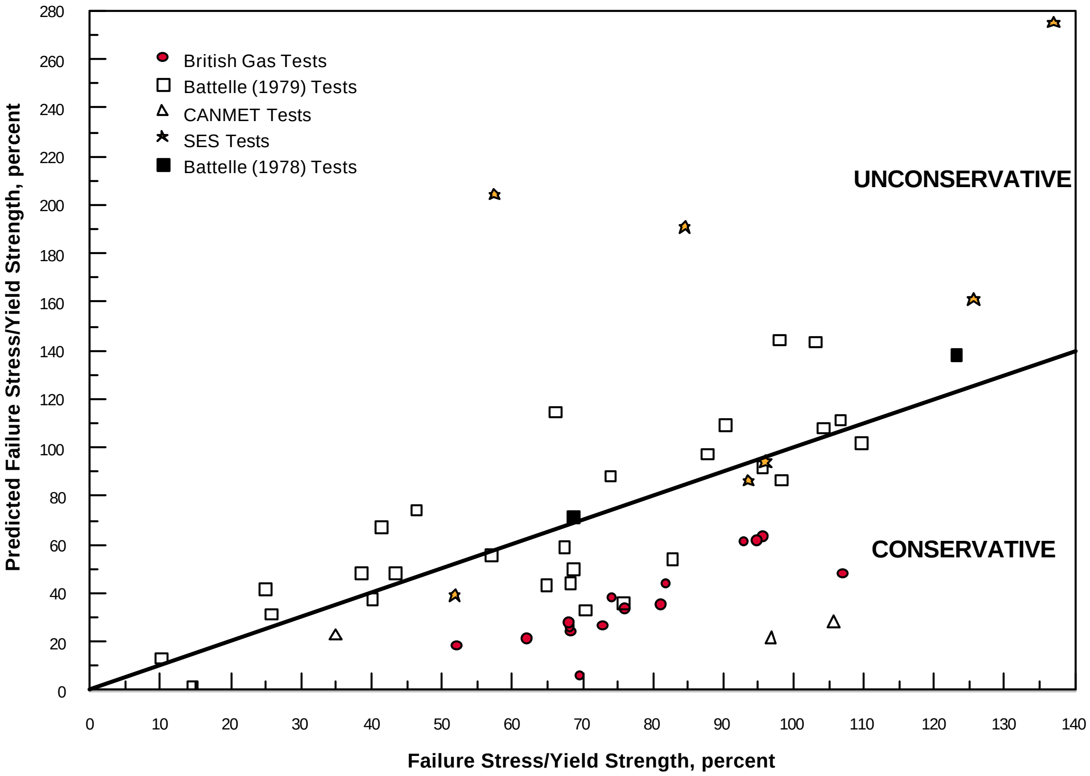


Figure 7 – Failure stress of dent and gouge defects predicted using the empirical *Q* factor model

7.2.2 The Dent-Gouge Fracture Model

The dent-gouge fracture model is based on a collapse modified strip-yield model. It includes expressions for the stress state at the base of the dent, and considers the interaction between fracture and plasticity.

The dent-gouge defect is modelled as an axially orientated, continuous dent (of constant width) with a single, infinitely long, axially orientated, sharp notch located at the base of the dent. The length of the dent or the gouge is not considered. The model was calibrated using the results of 111 ring and 21 vessel burst tests of smooth dents containing machined notches (notch then dent) introduced at zero pressure carried out by British Gas[10]. A relationship between the implied fracture toughness and the upper shelf Charpy impact energy was determine from a non-linear regression analysis of the dent and gouge test data (and as such, the correlation between Charpy energy and fracture toughness in the model is not generally applicable).

The dent-gouge fracture model is defined as follows (in SI units)

$$\frac{s_q}{\bar{s}} = \frac{2}{p}\cos^{-1}\left[\exp-\left\{113\frac{1.5pE}{\bar{s}^2 Ad}\left[Y_1\left(1-1.8\frac{H_o}{D}\right)+Y_2\left(10.2\frac{R}{t}\frac{H_o}{D}\right)\right]^{-2}\exp\left[\frac{\ln(0.738C_v)-K_1}{K_2}\right]\right\}\right]$$

Equation 9

where

$$\bar{s} = 1.15 s_Y\left(1-\frac{d}{t}\right)$$ **Equation 10**

$$Y_1 = 1.12 - 0.23\left(\frac{d}{t}\right) + 10.6\left(\frac{d}{t}\right)^2 - 21.7\left(\frac{d}{t}\right)^3 + 30.4\left(\frac{d}{t}\right)^4$$ **Equation 11**

$$Y_2 = 1.12 - 1.39\left(\frac{d}{t}\right) + 7.32\left(\frac{d}{t}\right)^2 - 13.1\left(\frac{d}{t}\right)^3 + 14.0\left(\frac{d}{t}\right)^4$$ **Equation 12**

$$K_1 = 1.9$$ **Equation 13**

$$K_2 = 0.57$$ **Equation 14**

$$H_o = 1.43 H_r$$ **Equation 15**

The flow stress assumed in the dent-gouge fracture model is not appropriate for higher grade steels (greater than X65), due to the increasing yield to tensile ratio with line pipe grade.

The dent-gouge fracture model is based on tests in which the damage was introduced at zero pressure, and the dent depth is that after spring back and measured at zero pressure. Therefore, a correction must be made for dents introduced at pressure and measured at pressure. An empirical rerounding correction factor developed by the EPRG is proposed (equation 13)[7]. This correction factor relates the dent depth (after the removal of the indenter) measured at pressure to that measured at zero pressure, for dents introduced at pressure. It is worth noting that this empirical correction is based on limited test data, and that alternative methods have been developed which should be more robust (e.g. Rosenfeld (1998)[44]), although there is limited test data available to validate such methods and they require more information than is given in the relevant published tests.

There have been no burst tests which have directly compared the effect of denting at pressure and denting at zero pressure on the failure behaviour of a smooth dent containing a gouge. Consequently, correcting for denting at pressure remains an area of considerable uncertainty.

Figure 8 shows a comparison between the predictions made using the semi-empirical dent-gouge fracture model and the published full scale test data.

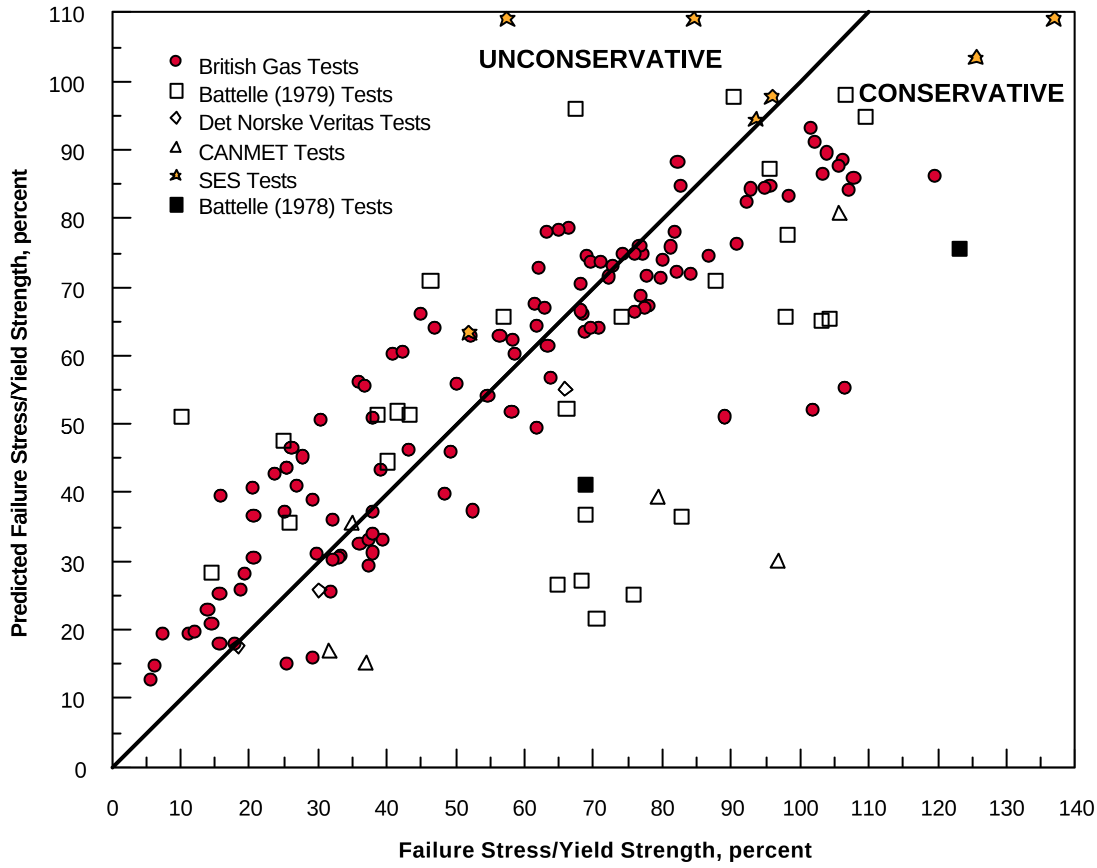


Figure 8 – Failure stress of dent and gouge defects predicted using the semi-empirical dent-gouge fracture model

7.3 Comparison with Test Data

The empirical *Q* factor model and the dent-gouge fracture model are compared against the published test data in order to determine the 'best' method in terms of the quality of fit with the test data, and thence to determine a suitable model uncertainty.

The diversity of dent and gouge burst tests presents problems when attempting to rationally compare models for predicting the burst strength of this type of damage. Some of the published tests do not contain sufficient information (such as toughness or material properties) and so have been excluded. The wall thickness is either the nominal value or the actual value (if quoted). Tests involving transverse dents or tests in which the 'gouge' has been ground smooth have been excluded. Both the *Q* factor model and the dent-gouge fracture model are based on the dent depth after spring back measured at zero pressure, therefore tests in which this information is not given have also been excluded. The tests which have been included in the comparison are those conducted by British Gas (1982, 1989), Battelle (1979), DNV (1982), CANMET (1985, 1988), SES (1996), and Battelle (1978). Of these tests, only those of SES (1996) and Battelle (1978) are tests in which the dent was introduced at pressure.

The total number of full scale tests considered in the comparison is 162, including 93 ring tests and 69 vessel tests. The formulation of the *Q* factor model is such that if *Q* is less than 300 ftlbf in^{-1}, then the failure stress cannot be defined. Therefore, although the 'gouge' length is given for all of the 69 vessel tests, the *Q* factor model can only be applied to 55 vessel tests.

The statistics of the ratio of the actual failure stress to the predicted failure stress for the two models are given in Table 2. Two subsets of the test data are considered: in (1) all of the tests applicable to each model are considered, whilst in (2) the tests are limited to those to which the *Q* factor model can be applied, and two apparent outliers in the predictions of the *Q* factor model, one Battelle test and one British Gas test (see Figure 7) have been removed. The dent-gouge fracture model is clearly the better model, although there is a large amount of scatter in the predictions (significantly greater than that for the part-wall NG-18 equations, see above). Some of the scatter can be attributed to the different methods of testing and general experimental scatter, but it is also indicative of the limitations of the dent-gouge fracture model.

		mean	standard deviation	coefficient of variation	number of tests
(1)	fracture model	1.09	0.48	0.44	162
	Q factor	1.80	2.02	1.12	55
(2)	fracture model	1.23	0.64	0.52	55
	Q factor	1.45	0.88	0.61	53

Note : (1) all tests, (2) limited number of tests (refer to text)

Table 2 – Statistical analysis of predictions made using the semi-empirical dent-gouge fracture model (EPRG) and the empirical *Q* factor model (PRC)

7.4 Recommendation in the Pipeline Defect Assessment Manual

The Pipeline Defect Assessment Manual recommends the dent-gouge fracture model for assessing the burst strength of a smooth dent containing a single, axially orientated gouge.

The dent-gouge fracture model does not give a lower bound estimate of the burst strength of a gouge, accordingly a 'model uncertainty' has been derived from the prediction interval for the classical least squares linear regression model, and this lower bound expression is given in PDAM. The effect of applying a confidence interval corresponding to a 95 percent one-tail confidence level is illustrated in Figure 9.

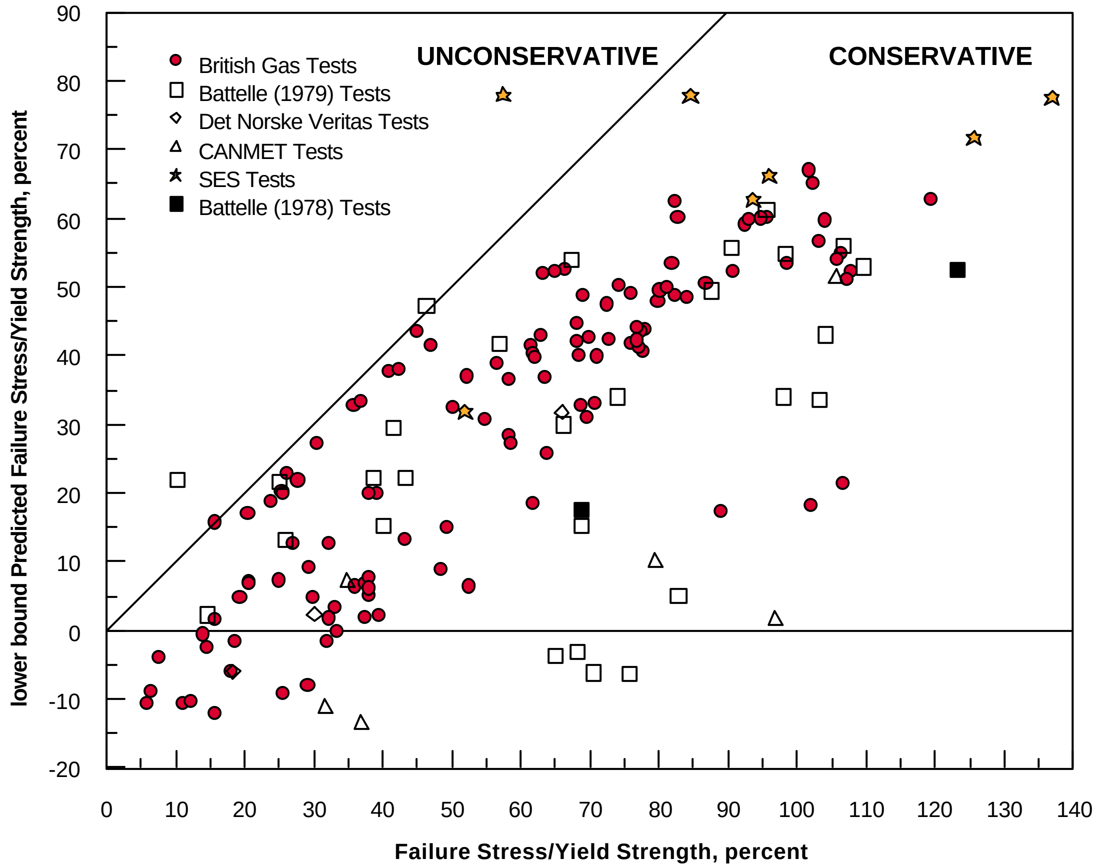


Figure 9 – Failure stress of dent and gouge defects predicted using a lower bound to the dent-gouge fracture model

7.5 Range of Applicability

The dent-gouge fracture model has been compared against the results of 165 full scale burst tests of rings and vessels containing dent-gouge defects or dent-notch defects. The range of the test data included in the comparison is given below (in SI units). This gives an indication of the range of applicability of the dent-gouge fracture model.

Pipe Diameter, mm	216.3	to	1066.8
Wall Thickness, mm	4.8	to	20.0
$2R/t$ ratio	33.6	to	107.7
Grade (API 5L)	X42	to	X65
Yield strength, Nmm^{-2}	279.2	to	543.3
Tensile strength, Nmm^{-2}	475.0	to	701.2
yield to tensile ratio	0.61	to	0.87
2/3 Charpy Impact Energy, J	16.3	to	130.7
Dent Depth, mm	1.5	to	146.5
$H/2R$	0.42	to	18.0
Defect Depth (d), mm	0.18	to	6.1
d/t	0.014	to	0.51
Defect Length ($2c$), mm	50.8	to	810.0
$2c/(Rt)^{0.5}$	0.84	to	8.98
Burst Pressure, Nmm^{-2}	0.972	to	25.24
Burst Stress (percent SMYS)	7.05	to	151.5

8 PIPELINE INTEGRITY AND PIPELINE INTEGRITY MANAGEMENT

8.1 Integrity

A fitness-for-purpose assessment of a pipeline defect will not, on its own, ensure continuing pipeline integrity. This is because pipeline integrity is ensuring a pipeline is safe and secure. It involves all aspects of a pipeline's design, operation, inspection, management and maintenance. This presents an operator with a complex 'jigsaw' to solve if they are to maintain high integrity.

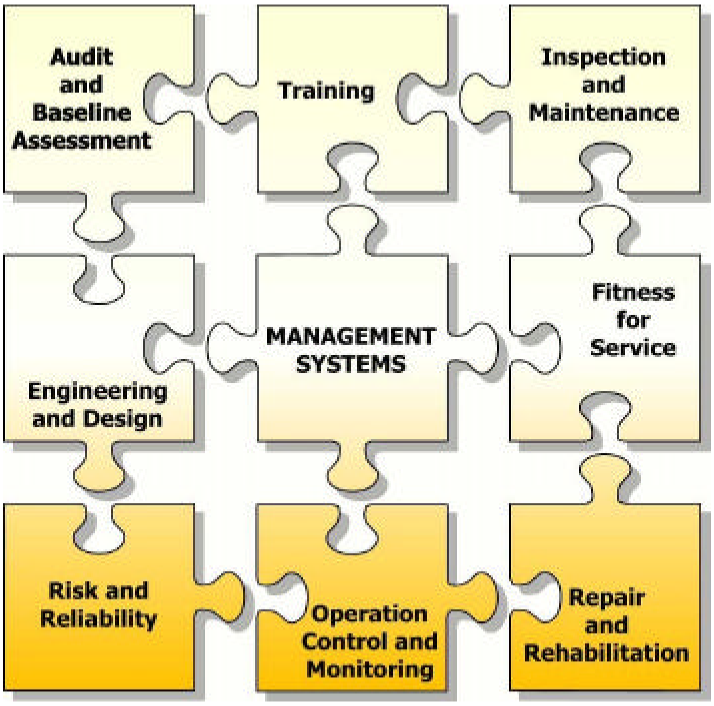


Figure 10 – The Key Elements of Pipeline Integrity Management

The key elements of pipeline integrity are given in Figure 10, and include:

- a highly trained workforce,
- good engineering, design, operation,
- inspection and maintenance,
- fitness-for-purpose assessment, and
- an appreciation of the risks associated with a pipeline, particularly as it ages.

These key elements are all contained and controlled via a formal pipeline management system.

8.2 Integrity Management

Pipeline integrity management is the management of all the elements of this complex jigsaw; the management brings all these pieces of the jigsaw together.

The American Petroleum Institute (API) is developing an industry consensus standard that gives guidance on developing Integrity Management Programmes (API 1160). This standard is expected to be published in late 2001 or early 2002. The American Society of Mechanical Engineers (ASME) is also developed a Integrity Management Appendix for ASMEB31.8, due for publication in February 2002[45].

9 THE NEED FOR AN 'HOLISTIC' APPROACH TO PIPELINE DEFECT ASSESSMENT AND INTEGRITY

This paper has reported a major new industry document that will assist pipeline companies to apply current best practices to the assessment of defects in their pipelines.

It is important to end by emphasising the need for an 'holistic' approach to pipeline defect assessment and integrity. Pipeline failures are usually related to a breakdown in a 'system', e.g. the corrosion protection 'system' has become faulty, and a combination of ageing coating, aggressive environment, and rapid corrosion growth may lead to a corrosion failure. This type of failure is not simply a 'corrosion' failure, but a 'corrosion control system' failure. Therefore, an engineer must appreciate the system in order to prevent failure; understanding the equation that quantifies failure pressure is just one aspect. Additionally, failures affect the surrounding people and the environment; therefore, an appreciation of the consequences of failure is essential. This means an understanding of risk analysis.

An 'holistic' approach to pipeline integrity is needed. The Pipeline Defect Assessment Manual is but one piece of the Pipeline Integrity 'Jigsaw' that must be solved so as to be able to manage a pipeline effectively and safely.

10 ACKNOWLEDGMENTS

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