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THE PIPELINE DEFECT ASSESSMENT MANUAL

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ABSTRACT

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 a 'fitness-for-purpose' assessment to determine whether or not to repair the pipeline. Consequently, the past 40 years has seen a large number of full scale tests of defects in pipelines, and the development of a number of methods for assessing the significance of defects. Some of these methods have been incorporated into industry guidance, others are to be found in the published literature. 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.

To address this industry need, a Joint Industry Project has been sponsored by fifteen international oil and gas companies[1] to develop a Pipeline Defect Assessment Manual (PDAM). PDAM documents the best available techniques currently available for the assessment of pipeline defects (such as corrosion, dents, gouges, weld defects, etc.) 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. It is intended to be another tool to help pipeline engineers maintain the high level of pipeline safety.

In addition to identifying the best methods, PDAM has served to identify a number of limitations in the current understanding of the behaviour of defects in pipelines, and the empirical limits in the application of existing methods. This paper discusses the PDAM project, in the context of both the current best practice available for defect assessment and the limitations of current knowledge.

1. INTRODUCTION

The most common causes of damage and failures in onshore and offshore, oil and gas transmission pipelines in Western Europe and North America are external interference (mechanical damage) and corrosion. Accordingly, the behaviour of defects in pipelines has been the subject of considerable study over the past 40 years, with a large number of full scale tests, analyses and other work having been undertaken. Many different fitness-for-purpose methods have been developed.

Fitness-for-Purpose. Fitness-for-purpose, as discussed here, means that a particular structure is considered to be adequate for its purpose, provided the conditions to reach failure are not reached[1]. Note that fitness-for-purpose may also have a legal and contractual meaning in different countries. Fitness-for-purpose is based on a detailed technical assessment of the significance of the defect. Local and national legislation and regulations may not permit certain types of defects to be assessed by fitness-for-purpose methods or may mandate specific limits. Such issues should always be considered prior to an assessment.

Safety must always be the prime consideration in any fitness-for-purpose assessment. It is always necessary to appreciate the consequences of a failure. These will influence the necessary safety margin to be applied to the calculations.

[1] 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.

Pipeline Integrity Management. 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. Similar observations can be drawn for failures due to external interference, stress corrosion cracking, etc..

These considerations lead to the conclusion that a 'holistic' approach to pipeline defect assessment and integrity is necessary; understanding the equation that quantifies the failure load is only one aspect.

Pipeline integrity management is the general term given to all efforts (design, construction, operation, maintenance, etc.) directed to wards ensuring continuing pipeline integrity. The American Petroleum Institute (API) has developed an industry consensus standard that gives guidance on developing integrity management programmes (API 1160)[2]. The American Society of Mechanical Engineers (ASME) is also developing an integrity management appendix for ASME B31.8[3].

The Pipeline Defect Assessment Manual. The Pipeline Defect Assessment Manual (PDAM) presents a considered view of the 'best' currently available methods for assessing the fitness-for-purpose of defects in pipelines. It is based on a critical review of the published fitness-for-purpose methods and test data. PDAM intended to be a document that will assist in maintaining pipeline integrity. The PDAM project is due for completion in August 2002. PDAM will be made available to the pipeline industry.

This paper summarises the methodology and gives an outline of the contents of PDAM. The best methods for assessing a variety of different types of defect are summarised (see Table 3). Empirical toughness limits derived from published test data are given and the assessment of external interference (dents and gouges) is described in more detail. The PDAM recommendations for the assessment of other types of defect will be described in future papers.

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)
$\overline{\sigma}$	flow stress (Nmm^{-2})
σ_θ	hoop stress at failure (Nmm^{-2})
σ_Y	yield strength (Nmm^{-2})
σ_U	ultimate tensile strength (Nmm^{-2})

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

The fitness-for-purpose of a defect in a pipeline may be determined by a variety of methods ranging from previous relevant experience (including workmanship acceptance levels), to model testing, to 'engineering critical assessments' (ECAs), where a defect is appraised analytically.

2.1 GENERIC

Various technical procedures are available for assessing the significance of defects in a range of structures. These methods use a combination of fracture mechanics and limit state (plastic collapse) methods. Both BS 7910 : 1999[1] and API RP 579[4] contain detailed engineering critical assessment methods which can be applied to defects in pipelines (although the latter document is biased towards defects in process plant).

2.2 PIPELINE-SPECIFIC

Documents such as the above 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). These pipeline specific methods are usually based on experiments, sometimes with limited theoretical validation; they are semi-empirical methods. Consequently, the methods may become invalid if they are applied outside their empirical limits. Accordingly, PDAM has considered the limits of the experimental validation of commonly used pipeline specific methods.

Methods and guidelines developed by the pipeline industry range from the NG-18 equations[5] (which formed the basis of methods such as ASME B31G [6] and RSTRENG[7]) and the Ductile Flaw Growth Model (DFGM) (implemented as PAFFC (Pipe Axial Flaw Failure Criteria))[8,9] 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[10], mechanical damage[11] and ductile fracture propagation[12] produced by the European Pipeline Research Group (EPRG).

The conservatism of generic methods compared to pipeline specific methods can largely be attributed to issues of constraint and ductile tearing. Constraint is the restriction of plastic flow in the vicinity of the crack tip due to stress triaxiality. Stress triaxiality is induced by load and geometry. The standard test methods used to measure fracture toughness are designed to give conditions of high constraint at the crack tip to ensure conservative results. Pipelines have low constraint because they are thin walled (geometry) and are predominantly subject to membrane tensile loading (loading mode). Conventional (single parameter) fracture mechanics does not consider the elevation in fracture toughness due to a reduction in the level of constraint, and hence an inherent margin of safety is included when applied to low constraint structures. The semi-empirical pipeline specific methods consider constraint implicitly because they have been developed from full scale tests in which these effects manifest themselves directly. Similarly, the increase in toughness with ductile crack growth (a rising resistance curve) is also considered implicitly. The difference between pipeline specific and generic methods diminishes when sophisticated fracture mechanics (two-parameter fracture mechanics, tearing analysis, etc.) and limit state methods are applied.

2.3 A BRIEF HISTORY OF PIPELINE DEFECT ASSESSMENT

i. The Early Days….

Fracture mechanics is the science of why things fail. The effect of defects on structures was studied qualitatively as long ago as the 15th century by Leonardo da Vinci; he measured the strength of lengths of iron wire, illustrating the effect of flaws on strength and observing that short wires were stronger than long wires (due to the lower probability of the shorter wire containing a defect). Notched bar impact testing of iron and steel was widely used by the end of the 19 th century to determine ductile to brittle transition temperatures[13].

In 1920, Griffith published a quantitative relationship between the fracture stress and the size of a flaw, derived in terms of a simple energy balance from a stress analysis of an elliptical hole by Inglis and the First Law of Thermodynamics. However, the work of Griffith was only applicable to perfectly elastic materials (brittle materials) and efforts to apply the theory to metals were initially not successful.

ii. The Start….

Prior to circa 1950, failure reports of engineering structures did not usually consider the presence of cracks. Cracks were considered unacceptable in terms of quality, but were not considered quantitatively. There were exceptions: the Liberty Ship failures (during the Second World War) are commonly cited as one of the prime instigators for the further development of the science of fracture mechanics.

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 plants. This lead to the development of fracture mechanics using various approaches (stress intensity factor (K), J-integral and crack tip opening displacement (δ)). The 1950s and 1960s was also a period where the safety of transmission pipelines was of interest, primarily in the USA due to its large and aging pipeline system.

iii. The Pipeline Pioneers….

Workers at the Battelle Memorial Institute in Columbus, Ohio extensively studied the failure of defects in line pipe steel through both theoretical work and full scale testing, under the auspices of the then Pipeline Research Committee of the American Gas Association. The principal objective of this early work was to provide a sound and quantitative technical understanding of the relationship between the hydrostatic test level and the number and size of defects removed. The concept of the flow stress was introduced and a correction for plasticity at the crack tip, required when applying linear-elastic fracture mechanics theory to elastic- plastic materials, was proposed[14,15].

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

1. 'Toughness dependent' failures – to predict the failure stress of these tests a measure of the fracture toughness was required (the critical stress intensity factor, K_c, or an empirical correlation with the upper shelf Charpy V-notch impact energy).
2. 'Flow stress dependent' ('plastic collapse') failures – to predict the failure stress of these tests only a measure of the strength of the material 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[5]. A summary of the test data and the transition from toughness to flow stress dependent failure is given in Fig. 1. The underlying expressions and concepts are still widely used today.

The original work and models accounted for the very complex failure process of a defect in a pipeline, involving bulging of the pipe wall, plastic flow, crack initiation and ductile tearing, although much of this is implicit and follows from the semi-empiricism. These pioneering models were safe due to inherently conservative assumptions and verification via full scale testing, but they are limited by the range of the experiments (generally, thin walled, lower grade, low yield to tensile ratio line pipe). The DFGM, developed by Battelle in the early 1990s, is a revision and update of the original NG-18 equations and better describes the significance of toughness, ductile tearing and plastic collapse[8,9].

iv. The Future….

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. With some notable exceptions, there has been little fundamental work reported, and this is a major, serious and somewhat puzzling omission. There has been a focus on developing 'patches' to existing methods, and of proving that these old methods are either (1) highly conservative, or (2) applicable to newer materials or applications via simple testing or numerical analysis.

These are ultimately short-sighted approaches to solving problems; rather effort should be directed towards the fundamental reasons why the older methods do not work (or are conservative) and to developing new methods. It is unreasonable to expect that 30 year old methods developed for thin wall, moderate toughness line pipe steels will be applicable to newer steels of higher strength (grade X100 or above) and toughness, larger diameter, thicker wall (deep water pipelines are approaching 50 mm in thickness), higher strains (deep water and arctic conditions (frost heave) will give rise to greater than 1 percent plastic strains). The original flow stress dependent methods were not conservative (see Fig. 1), and they, and the methods that were based on them, are not necessarily theoretically applicable to newer, thicker materials.

The pioneering work in the 1960s and 70s made use of 'leading edge' knowledge of fracture mechanics, and this fundamental research was actively supported by the pipeline industry. Whether this can be said of the industry at the start of the 21st century is another matter. Such a failing will impede the development of new design and integrity solutions (high grade, high pressure, high stress, high strain, etc.).

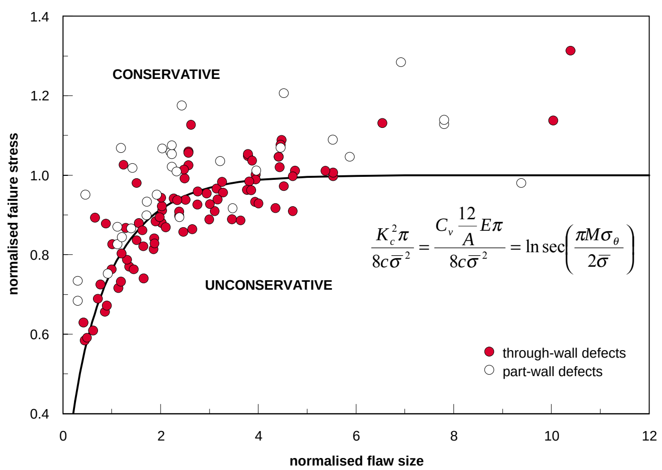

Fig. 1 The NG-18 equations and test data, illustrating flow stress and toughness dependent behaviour[2]

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, defines the necessary input data, gives the limitations of the method, and defines an appropriate factor to account for the model uncertainty. The model uncertainty for each assessment method has been derived from a statistical comparison of the predictions of the method with the published test data, based on the prediction interval of the classical linear regression model.

PDAM provides the written text, the methods, recipes for application, acceptance charts and simple examples, and is supported by 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 Fig. 9.

PDAM has been closely scrutinised throughout its development by the sponsors, and all literature reviews and chapters of the manual have been independently reviewed by international experts in the field of pipeline defect assessment.

PDAM does not present new defect assessment methods; it 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 of the current state of knowledge.

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 defects, and the assessment of defects in pipe fittings (pipe work, fittings, elbows, etc.). Guidance is also given on predicting the behaviour of defects upon penetrating the pipe wall (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. There are some combinations of defect type, orientation and loading for which there are no clearly defined assessment methods. In summary, the assessment of defects subject to static or cyclic internal pressure loading is well understood, but, in general, other loads and combined loading are not.

5. THE LAYOUT OF THE PIPELINE DEFECT ASSESSMENT MANUAL

The Pipeline Defect Assessment Manual 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.

[2] The equation is the toughness dependent through-wall failure criterion, expressed in imperial units[5].

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 to alternative sources of information available in national or international guidance, codes or standards.

The flow charts included for each defect type 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 is given in Fig. 10.

6. ASSESSMENT METHODS IN THE PIPELINE DEFECT ASSESSMENT MANUAL

A summary of all of the methods recommended in the Pipeline Defect Assessment Manual for predicting the burst strength of a defect subject to internal pressure is given in Table 3. Longitudinally and circumferentially orientated defects are considered. The 'primary' methods (indicated in normal font) are plastic collapse (flow stress dependent or limit state) failure criteria, and are only appropriate if a minimum toughness is attained (see below). The secondary methods (indicated in *italic font*) are the alternative methods recommended when a minimum toughness is not attained. Upper shelf behaviour is assumed throughout. The general procedures for assessing flaws in structures, based on fracture mechanics, given in BS 7910 (and API 579) can be applied in general (irrespective of upper or lower shelf behaviour), but will generally be conservative compared to the pipeline specific methods[3].

Having given an overview of the contents of PDAM, the remainder of this paper (1) describes the role of toughness and gives empirical toughness limits for the application of flow stress dependent assessment methods, and (2) gives specific guidance on the assessment of gouges and dents and gouges.

7. TOUGHNESS LIMITS

Line pipe steels is generally tough and ductile, and operates on the upper shelf[4]. Initiation and propagation of a part-wall flaw through the wall occurs under a ductile fracture mechanism, involving some combination of plastic flow and crack initiation and ductile tearing, involving a process of void nucleation, growth and coalescence. The relative importance of plastic flow and crack initiation and tearing depends on the toughness of the material and the geometry of the defect. Fig. 2 is an illustration of the role of toughness in the failure of a part-wall defect.

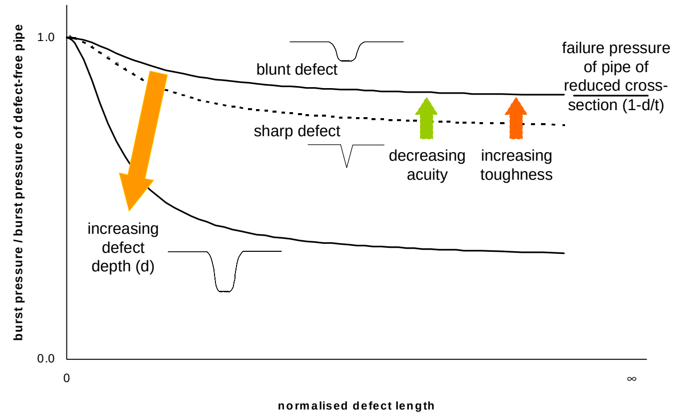

Fig. 2 The effect of material toughness, defect depth, length and acuity on burst strength

As the toughness decreases the burst strength of a defect will decrease. As the toughness increases the burst strength of a defect will increase, but tending towards an upper limit corresponding to the plastic collapse limit state, where failure occurs due to plastic flow (and can be predicted using limit state methods). Therefore, if the toughness is greater than some minimum value then the failure of a defect will be controlled by plastic collapse and only knowledge of the tensile properties of the material is required to predict the burst strength (as demonstrated in the transition between the toughness dependent and flow stress forms of the NG-18 equations).

The upper bound to the strength of a material is the ultimate tensile strength. If failure is due to plastic collapse then the flow stress should be the ultimate tensile strength; failure will occur when the stress in the remaining ligament exceeds σ_U. The minimum toughness necessary to ensure that failure is controlled by plastic collapse may be high; Leis suggests a full size equivalent upper shelf Charpy impact energy of between 60 and 75 ftlbf (81 J and 102 J)[5] for a fully ductile response[16]. Considering Fig. 1 and Fig. 3, it is clear that flow stress dependent behaviour, as defined in the context of the NG-18 equations, manifests itself at a lower toughness.

This introduces an important distinction. A minimum toughness may be defined empirically above which a given

[3] PAFFC incorporates correlations between the fracture toughness and the upper shelf Charpy impact energy; therefore, PAFFC is not applicable to lower shelf conditions (although the underlying theoretical model is applicable if the fracture toughness (K, J or δ) is measured).

[4] Brittle (cleavage) fracture can occur in older line pipe steels or under unusual (typically upset) conditions which can cause low temperatures. If the DWTT (Drop Weight Tear Test) transition temperature is less than the minimum design temperature, then initiation will be ductile. A high upper shelf Charpy V-notch impact energy is also desirable to ensure that failure is controlled by plastic collapse[16-18]. The DWTT transition temperature is defined as the temperature at which a DWTT specimen exhibits 85 percent shear area. The steel is on the upper shelf if the DWTT transition temperature is less than the current temperature of the steel.

[5] The 2/3 thickness specimen size equivalent is between 54 J and 68 J.

'flow stress dependent' (or pseudo 'plastic collapse') failure criterion will give reasonably conservative predictions (taking into account experimental scatter). This is not equivalent to stating that failure is due to plastic collapse. The empirical minimum toughness may be lower than the true minimum toughness for plastic collapse because of the inherent conservatism in the flow stress dependent failure criterion (consider that flow stress dependent failure criteria typically define the flow stress as some function of σ_Y, or the average of σ_Y and σ_U, and implicitly consider some degree of ductile tearing (tearing was observed in the original full scale tests used to develop the NG-18 equations[14]).

Wall thickness is also important because of the transition from plane stress to plane strain behaviour and the increasing constraint with increasing wall thickness. Pipelines are typically thin walled structures (the wall thickness is seldom greater than 1 in. (25.4 mm)). A minimum toughness limit should be defined with respect to a maximum wall thickness. Defect acuity is also a consideration, blunt defects are less sensitive to toughness than sharp defects (blunt defects record higher burst strengths in low to moderate toughness steels).

Toughness Limits for the NG-18 Equations Empirical minimum toughness limits for the applicability of the flow stress dependent though-wall and part-wall NG-18 equations can be defined by reference to the results of relevant full scale burst tests (see section 8.1).

The effect of toughness on the accuracy of predictions of the burst strength of an axially orientated, machined, part-wall defect made with the flow stress dependent part-wall NG-18 equations is illustrated in Fig. 3. A flow stress of the average of σ_Y and σ_U and a two term Folias factor has been used (Eqs. (1) to (3), below). 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). Consequently, 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). 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. It is shown later in Fig. 5 that conservative predictions of the full scale tests can be obtained if this toughness limit is applied together with a suitable correction for the model uncertainty.

It is important to note that whilst this approach to deriving a toughness limit is simple and practical, it has the disadvantage of introducing further conservatism for higher toughness line pipe steels. Furthermore, it is not a limit for failure by plastic collapse, as defined by Leis (2001). A more sophisticated approach, such as PAFFC, would be more robust for a wider range of material toughness.

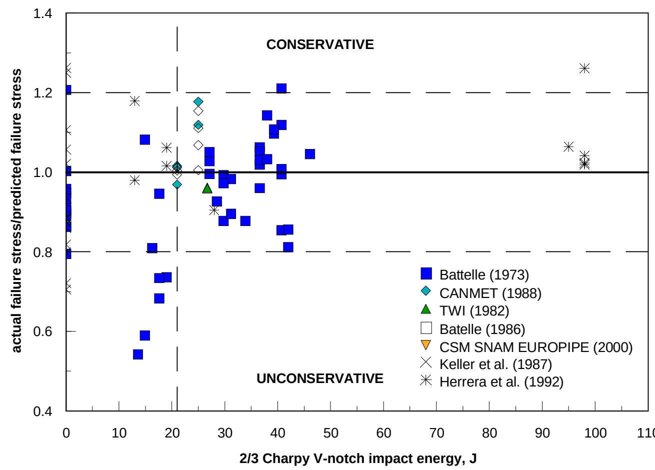

Fig. 3 The effect of toughness on predictions of part-wall burst tests made using the flow stress dependent part-wall NG-18 equation[6]

A similar analysis of burst tests of axially orientated, machined, 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. The maximum wall thickness is 21.9 mm. 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[17,18].

Range of Toughness from Published Data The minimum toughness (2/3 specimen thickness upper shelf Charpy V-notch impact energy) and maximum wall thickness derived from the published full scale test data for several types of defect are summarised below[7]. These values indicate the potential limits of the various assessment methods. The methods may be applicable outside of these limits, but there is limited experimental evidence. The results of specific studies of the range of validity of specific assessment methods are also indicated. In all cases, the basic assumption is that the line pipe steel is on the upper shelf.

Corrosion The lowest toughness is 18 J (13 ftlbf) and the maximum wall thickness is 22.5 mm (1.0 in.).

ASME B31G, modified B31G and RSTRENG are applicable to low toughness steels (on the upper shelf)[19,20]. The recently developed methods for assessing corrosion, such as DNV-RP-F101[21] and PCORRC[20] are only proven for moderate to high toughness steels; a minimum toughness of 41 J (30 ftlbf) has been proposed[20]. None of the methods for

[6] The toughness is not reported in a number of tests; these tests are shown in Fig. 3 as having zero toughness to indicate the range of the test data.
[7] Note that the Charpy impact energy is not reported for all of the tests.

assessing corrosion have been validated in line pipe with a wall thickness greater than 25.4 mm.

Gouges The lowest toughness is 14 J (10 ft lbf) and the maximum wall thickness is 21.7 mm (0.854 in.).

Changes to the local microstructure at the base of a gouge, as a consequence of the gouging process, have been studied by CANMET. It is indicated that the effect of such changes were not significant if the upper shelf Charpy V-notch impact energy (2/3 specimen size) exceeded 20 J [42]. The flow stress dependent part-wall NG-18 equation can be used to predict the burst strength of a gouge (see section 8). The minimum toughness to apply this method is 21 J (maximum thickness 21.7 mm), see above.

Dent and Gouge The lowest toughness is 16 J (12 ft lbf) and the maximum wall thickness is 20.0 mm (0.787 in.).

Dent The lowest toughness is 20 J (15 ft lbf) and the maximum wall thickness is 12.7 mm (0.500 in.).

8. THE ASSESSMENT OF THE BURST STRENGTH OF A GOUGE IN PDAM

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 may be in fully rerounded pipe (i.e. a dent of zero depth).

A gouge reduces the burst 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.

8.1 FULL SCALE BURST TESTS OF 'GOUGES'

A large number of full scale burst tests of longitudinally orientated 'gouges' (part-wall defects) in line pipe steel have been conducted by a number of different organisations. Tests in other pressure vessel steels have also been carried out. The total number of published burst tests is of the order of 190, although only the most relevant 115 tests are referred to here.

The tests can be variously described as follows[8]:

1. machined 'V-shaped' notch or slot (artificial gouge)
   - Battelle (1965 - 1974)[5] (vessels) (48 tests)
   - Battelle (1986)[22] (vessels) (3 tests)
   - British Gas (1974)[23] (vessels) (3 tests)
   - British Gas (1981, 1982)[24] (vessels) (1 test)
   - Iron and Steel Institute of Japan (Kubo et al.) (1993*)[25] (vessels) (19 tests)[9]
   - CSM SNAM EUROPIPE (2000)[26] (vessels) (2 tests)
2. scrape (gouge) the pipe using a tool bit mounted on a pendulum
   - CANMET (1985, 1988)[27,28] (vessels) (12 tests)
3. fatigue pre-cracked semi-elliptical machined notch
   - TWI (Garwood et al.) (1982)[29] (vessels) (2 tests)
   - TÜV and Mannesmann (Keller et al.) (1987) [30] (vessels) (15 tests)
   - University of Tennessee (Herrera et al.) (1992)[31] (vessels) (10 tests)

It is noteworthy that 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.

8.2 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 criteria (the NG-18 equations)[5]. The through-wall and part-wall criteria are semi-empirical. The 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. 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.

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[32,33]. The part-wall NG-18 equations are also recommended in the EPRG guidelines for the assessment of mechanical damage[11].

The flow stress dependent part-wall NG-18 equation is as follows

$$\sigma_\theta = \bar{\sigma}\left[\frac{1-\frac{d}{t}}{1-\frac{d}{t}\left(\frac{1}{M}\right)}\right] \qquad (1)$$

$\bar{\sigma}$ is the flow stress, which is an empirical concept intended to represent the stress at which unconstrained plastic flow occurs in a strain hardening elastic-plastic material via a single parameter. One commonly used definition of the flow stress is[10]

$$\bar{\sigma} = \frac{\sigma_Y + \sigma_U}{2} \qquad (2)$$

M is the Folias factor, representing the stress concentration due to the bulging that occurs under internal pressure loading. The

[8] The tests marked with an asterisk have not been included in the statistical comparison of the two methods.

[9] Note that there is a large difference between the test temperature and the temperature at which the material properties were measured.

[10] ASME B31G uses a flow stress of 1.1 times the yield strength, modified B31G and RSTRENG (and the NG-18 equations) use a flow stress of the yield strength plus 10 ksi (68.95 Nmm^{-2}).

analytical solution for the Folias factor is an infinite series. Three commonly used approximations are given below.

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

$$M = \sqrt{1 + 0.314\left(\frac{2c}{\sqrt{Rt}}\right)^2 - 0.00084\left(\frac{2c}{\sqrt{Rt}}\right)^4} \tag{4}$$

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

Equation (5) is the expression that appears in ASME B31G. It is the most conservative approximation. Equation (4) appears in modified B31G and RSTRENG. Equation (3) is a close approximation to Eq. (4) that is valid for $2c/(Rt)^{0.5}$ greater than 8.0.

The growth through wall of a sharp, part-wall defect in ductile line pipe occurs through some combination of plastic flow and ductile tearing. The NG-18 equations do not explicitly consider the effects of ductile tearing on the failure of through-wall and part-wall defects. A more sophisticated method for assessing part-wall defects, such as gouges, is PAFFC[9].

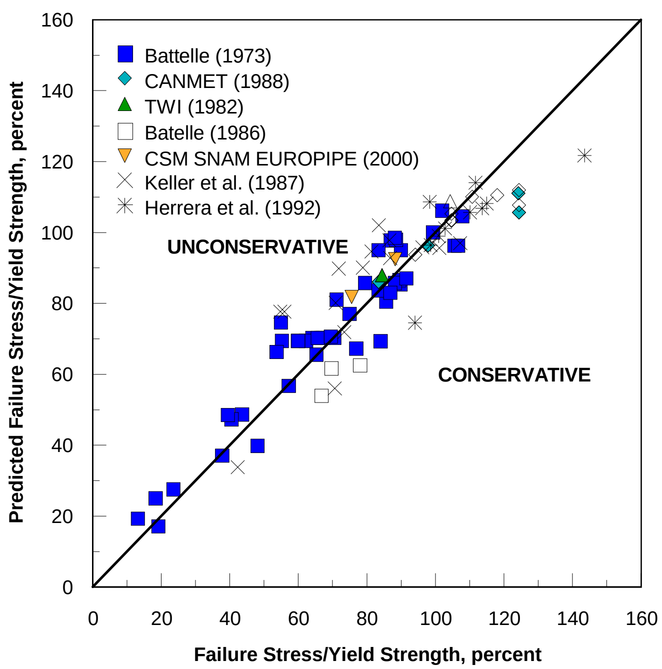

Fig. 4 Failure stress of axially orientated part-wall defects predicted using the part-wall NG-18 equation

8.3 COMPARISON WITH TEST DATA

The flow stress dependent form of the part-wall NG-18 equations 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). Consequently, 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 section 7, above) 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 are given in Table 1.

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

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

There is little difference between the three forms of the Folias factor, the approximate two term factor (Eq. (3)) and the three term factor (Eq. (4)) being almost identical; similarly for a flow stress of the average of σ_Y and σ_U, and one of σ_Y plus 10 ksi (as quoted in Kiefner et al. (1973)). A flow stress equal to σ_U gives, on average, non-conservative predictions, and a slight increase in the scatter. A comparison between the predictions made using the NG-18 equation, with a flow stress of the average of σ_Y and σ_U and the two term Folias factor (Eqs. (1) to (3)), and the published full scale test data is shown in Fig. 4.

8.4 RECOMMENDATION IN PDAM

PDAM 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 (Eqs. (1) to (3)). 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.

The part-wall NG-18 equation does not give a lower bound estimate; accordingly, a 'model uncertainty' has been derived. The effect of applying a confidence interval corresponding to a 95 percent one-tail confidence level is illustrated in Fig. 5; note that all of the tests with a toughness greater than 21 J are conservatively predicted.

When assessing a gouge it is important to consider the possibility of cracking at the base of the gouge and the presence of a dent. An assessment can be non-conservative if these issues are not considered. This may mean that it is necessary to excavate the pipeline to perform a detailed inspection of the damage. It is suggested 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.

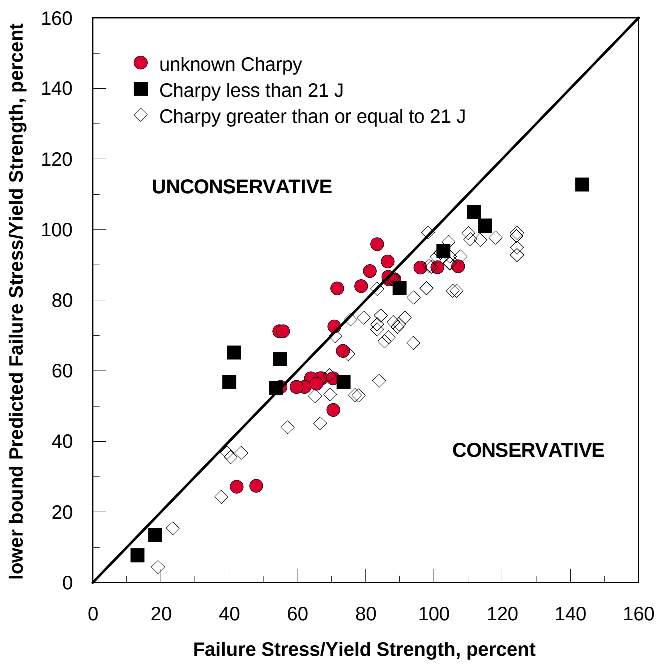

Fig. 5 Failure stress of axially orientated part-wall defects predicted using a lower bound to the part-wall NG-18 equation

8.5 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 equation.

Pipe Diameter, mm	114.0	to	1422.4
Wall Thickness, mm	5.6	to	21.7
$2R/t$ 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

9. THE ASSESSMENT OF THE BURST STRENGTH OF A DENT AND GOUGE IN PDAM

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 type 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

9.1 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. A variety of different test methods have been used, as indicated below. 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.

The tests can be variously described as follows[11]:

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 (1982, 1989) [24,34] (108 ring tests and 23 vessel tests)
   - Tokyo Gas (1998*)[35] (vessels) (3 tests)
2. damage introduced at zero pressure; machine a 'V-shaped' notch (artificial gouge) and then introduce the dent
   - Battelle (1979, 1986)[22,36-38,39] (vessels) (30 tests)
   - Nanyang Technical University (1992*)[40] (vessels) (17 tests)
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 (2000)[41] (vessels) (1 test)
4. damage introduced at zero pressure; introduce the dent and then scrape (gouge) the pipe using a tool bit mounted on a pendulum

[11] The tests marked with an asterisk have not been included in the statistical comparison of the two methods.

- CANMET (1985, 1988)[28,42] (vessels) (11 tests)

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)[43,44] (vessels) (14 tests)
6. damage (dent) introduced at pressure; gouge at zero pressure and then introduce the dent at pressure
   - EPRG (1991*, 1992*)[45,46] (vessels) (8 tests)
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)[36] (vessels) (2 tests)
8. damage introduced at pressure; dent and gouge introduced simultaneously using a specially designed test rig
   - British Gas (1983*)[47] (vessel) (1 test)
   - Battelle (1986*)[22,39] (vessels) (17 tests)
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*)[48,49] (vessels) (10 tests)
10. damage introduced at pressure; machine a blunt (rounded) notch at zero pressure and then introduce the dent at pressure
    - University of Cambridge (1992*, 1993*, 1996*)[50-52] (vessels) (20 tests)
11. damage introduced at zero pressure; machine a 1 in. wide slot (artificial corrosion) and then introduce the dent
    - SES (1997*)[53] (vessels) (3 tests)

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[22]. 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). Introducing the dent after the gouge increases the likelihood of cracking occurring at the base of the gouge. The most realistic tests are those in which the dent and gouge are introduced into pressurised pipe under dynamic conditions.

9.2 METHODS FOR PREDICTING THE BURST STRENGTH OF A DENT AND GOUGE

The behaviour of a dent containing a gouge is complex. A dent and gouge is a geometrically unstable structure. The base of the gouge may contain cracking and the properties of the material in the dent and gouge may have been adversely affected. Outward movement of the dent promotes initiation and growth of cracking in the base of the gouge, changing the compliance of the dent and gouge structure. 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)[54,55].

Empirical relationships for predicting the burst strength of a smooth dent containing a gouge have been proposed by British Gas [24,47], the EPRG[11] and Battelle[22,37]. A semi-empirical fracture model for assessing the burst strength of a dent-gouge defect has been developed by British Gas[56], and has subsequently been included in the EPRG recommendations for the assessment of mechanical damage[11]. More sophisticated models are under developed (e.g. Leis et al. (2000)), which attempt to more accurately model the failure mechanism of a dent and gouge defect.

The two most widely quoted models for predicting the failure stress of a dent and gouge defect are:

1. The empirical *Q* factor model developed by Battelle under the auspices of the Pipeline Research Council International (PRCI)[22,37].
2. The dent-gouge fracture model developed by British Gas and adopted by the EPRG[11,56].

Both of these models are based on the dent depth after spring back and measured at zero pressure.

The Empirical *Q* Factor Model Battelle developed an empirical model for predicting the burst strength of a smooth dent containing a gouge based on the results of 30 full scale burst tests[22,36-38], in which the damage was introduced at zero pressure by notching and then denting the pipe. The failure stress, normalised by the flow stress, was related to an empirical parameter, denoted *Q*. 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)

$$\frac{\sigma_f}{\bar{\sigma}} = \frac{(Q-300)^{0.6}}{90} \quad (6)$$

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

$$\bar{\sigma} = \sigma_Y + 10000 \text{ psi} \quad (8)$$

Fig. 6 shows a comparison between the predictions made using the empirical *Q* factor model and the published full scale test data.

The Dent-Gouge Fracture Model 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 elevated membrane and bending stresses at the base of the dent are considered, through an approximate solution based on thin shell theory and Castigliano's second theorem. The underlying fracture model, considering the reaction between fracture (toughness) and plasticity, is a collapse modified strip-yield model. 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[24]. A relationship between the implied fracture toughness and the upper shelf Charpy impact energy (for a 2/3 size specimen) was determined from a non-linear regression analysis of the dent and gouge test data (therefore, the correlation between Charpy energy and fracture toughness is not generally applicable).

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

$$\frac{\sigma_\theta}{\bar{\sigma}} = \frac{2}{\pi}\cos^{-1}\left[\exp-\left\{113\frac{1.5\pi E}{\bar{\sigma}^2 Ad}\left[Y_1\left(1-1.8\frac{H_o}{D}\right)+Y_2\left(10.2\frac{R}{t}\frac{H_o}{D}\right)\right]^{-2}\exp\left[\frac{\ln(0.738C_v)-K_1}{K_2}\right]\right\}\right] \quad (9)$$

$$\bar{\sigma} = 1.15\sigma_Y\left(1-\frac{d}{t}\right) \quad (10)$$

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

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

$$K_1 = 1.9 \quad (13)$$

$$K_2 = 0.57 \quad (14)$$

$$H_o = 1.43H_r \quad (15)$$

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 (Eq. (13))[11]. 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) [57]), 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.

Fig. 7 shows a comparison between the predictions made using the semi-empirical dent-gouge fracture model and the published full scale test data.

9.3 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. A number of the tests can not be considered because of the absence of toughness, actual material properties or dent depth after spring back measured at zero pressure. Tests involving transverse dents or tests in which the 'gouge' has been ground smooth have also been excluded.

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 ft.lbf.in^{-1}, then the failure stress can not 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 of these tests.

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

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 (PRCI)

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 Fig. 6) have been removed. The dent-gouge fracture model is clearly the better model. Note that there is a larger amount of scatter in the predictions of dent-gouge tests compared to the predictions of gouges and notches in undented pipe using the part-wall NG-18 equation (see above) .

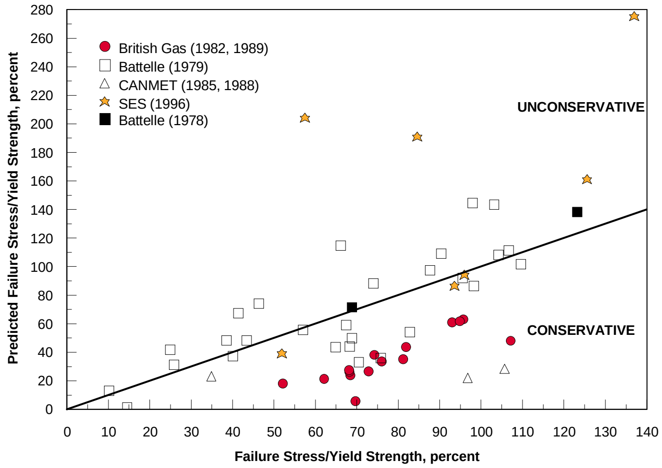

Fig. 6 Failure stress of dent and gouge defects predicted using the empirical *Q* factor model

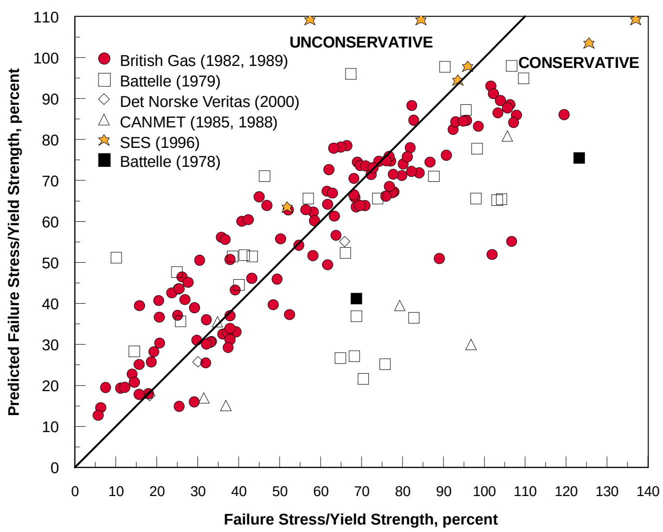

Fig. 7 Failure stress of dent and gouge defects predicted using the semi-empirical dent-gouge fracture model

9.4 RECOMMENDATION IN PDAM

PDAM 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 combined dent and gouge, accordingly a 'model uncertainty' has been derived. The effect of applying a confidence interval corresponding to a 95 percent one-tail confidence level is illustrated in Fig. 8.

The assessment of a dent and gouge defect is difficult. The morphology of the damage is such that ultrasonic inspection techniques may not be reliable. It is suggested that the measured depth of the gouge be increased by 0.5 mm, as discussed above.

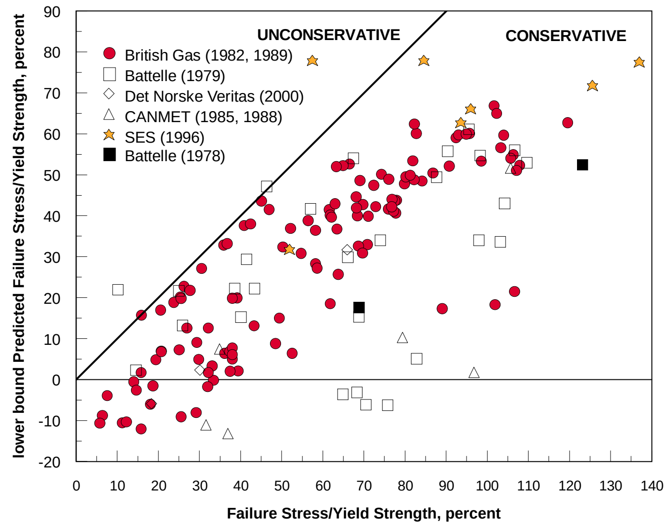

Fig. 8 Failure stress of dent and gouge defects predicted using a lower bound to the semi-empirical dent-gouge fracture model

9.5 RANGE OF APPLICABILITY

The dent-gouge fracture model has been compared against the results of 162 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
Notch Depth (d), mm	0.18	to	6.1
d/t	0.014	to	0.51
Notch 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, Nmm^{-2}	29.2	to	626.8
Burst Stress (percent SMYS)	7.05	to	151.5

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	internal pressure (static) longitudinally orientated	**internal pressure (static) circumferentially orientated**
corrosion	DNV-RP-F101[21] *modified B31G*[6,7] *RSTRENG*[7]	Kastner local collapse solution[58]
gouges	NG-18 equations[5] *PAFFC*[8,9] *BS 7910*[1] *(or API 579*[4]*)*	Kastner local collapse solution *BS 7910 (or API 579)*
plain dents	empirical limits	
kinked dents	no method[1]	
smooth dents on welds	no method	
smooth dents and gouges	dent-gouge fracture model[11,56]	no method
smooth dents and other types of defect	dent-gouge fracture model	no method
manufacturing defects in the pipe body[2]	NG-18 equations *BS 7910 (or API 579)*	Kastner local collapse solution *BS 7910 (or API 579)*
girth weld defects	-	workmanship, EPRG[10] *BS 7910 (or API 579)*
seam weld defects	workmanship *BS 7910 (or API 579)*	-
cracking	*BS 7910 (or API 579)* *PAFFC*	
environmental cracking[3]	*BS 7910 (or API 579)* *PAFFC*	
leak and rupture	NG-18 equations *PAFFC*	Schulze global collapse solution[59]

Note:

1. 'No method' represents both limitations in existing knowledge and circumstances where the available methods are too complex for inclusion in a document such as PDAM.
2. The term 'manufacturing defect' covers a wide range of pipe body defect (laminations, inclusions, seams, cold shuts, gouges, plug scores, pits, rolled-in slugs, etc.). Consequently, it may not be possible to characterise a manufacturing defect in the pipe body as a metal-loss or crack-like defect, it is then generally necessary to rely on workmanship limits and industry experience.
3. Environmental cracking (stress corrosion cracking, hydrogen blisters, hydrogen stress cracking, etc.) can be very difficult to assess and cannot necessarily be simply characterised as a crack-like defect.

Table 3 Recommended methods the Pipeline Defect Assessment Manual for assessing the burst strength of defects subject to static internal pressure loading

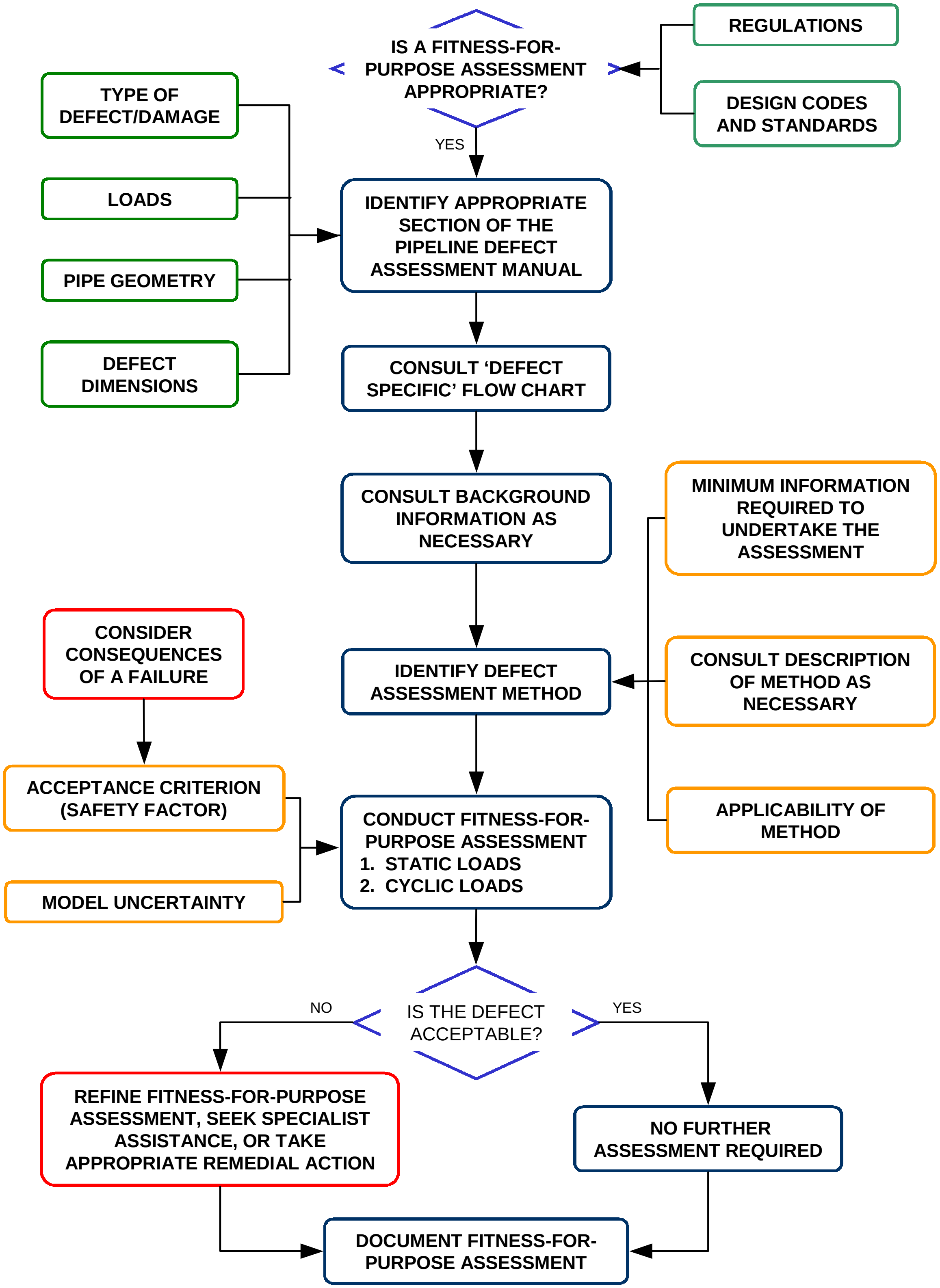

Fig. 9 The role of the Pipeline Defect Assessment Manual in the fitness-for-purpose assessment of a pipeline defect

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

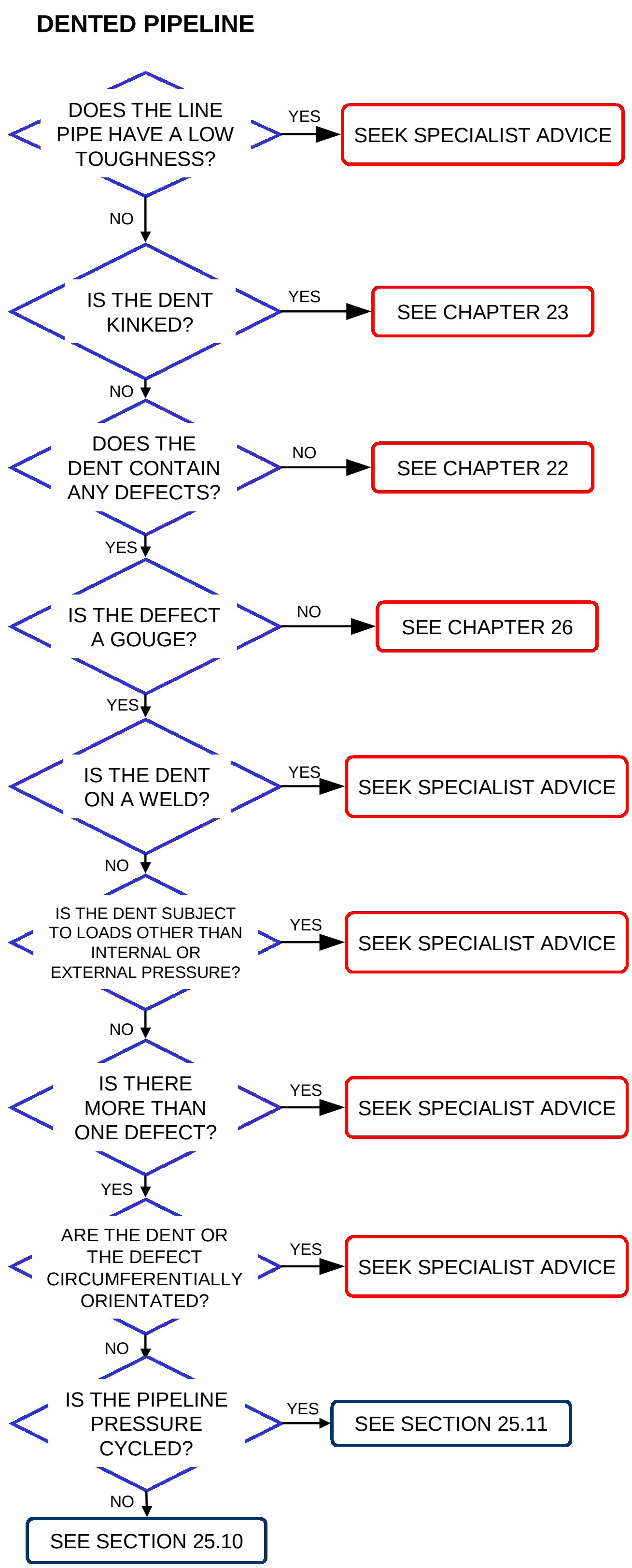

Fig. 10 The assessment of a smooth dent containing a gouge