Fracture Control of Steel Pipelines

A Code of Practice for the Australian Pipeline Industry

Second Edition (December 2021)

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Chapter 1 : Introduction

Pipelines for the transportation of fluids, such as water, oil, gas, and sewerage, play a significant role in global infrastructure.

Where a pipeline is used to move fluids long distances at high pressures, the most economic design is generally welded steel. Steel has the strength to contain high pressures with minimal material, and welds are able to provide a continuous structure, achieving a balance of costs that is more efficient over long distances than available alternatives.

The intent during pipeline design, construction and operation is that a pipeline will be free of defects; "defect", in this document, is defined as any flaw or blemish in the pipeline material. In reality, this objective cannot be achieved with confidence. Pipelines are long buried structures and undetected defects are always a possibility. Hence, it is necessary that the ability of the material to *tolerate* defects is understood and controlled.

Applying a defect tolerance mindset, pipeline design and construction aims to create a pipeline that is sufficiently free of defects to be 'fit for service', and operation of the pipeline will aim to detect and repair defects before they grow to the extent that the pipeline is no longer 'fit for service'.

Fracture mechanics is used in the pipeline industry to distinguish between the various consequences that may result from damage to the pipe. From a fracture perspective, four consequences are distinguished, shown in Figure 1-1.

A *surface defect* is the most minor type of damage, and includes dents, gouges or part-through-wall cracks that do not result in any loss of containment. A *leak* occurs when a defect penetrates the pipe wall and there is a release of the fluid being transported (this is a *stable* through-wall defect). *Rupture* will occur if the defect exceeds a critical length and is sufficient to fracture the pipeline. This is also called "full-bore" rupture, because the resulting hole provides no flow restriction to the fluid escaping from up- and down-stream (it is an *unstable* through-wall defect). *Running fracture* occurs if the rupture does not immediately arrest.

Consequently, fracture is an important consideration in the design of steel pipelines, whereby the conditions that cause a crack (a sharp defect) in a material both to begin growing and to stop growing are determined. In fracture mechanics, the process is described by the terms 'fracture initiation', 'fracture propagation', and 'fracture arrest'. For pressure-containing pipe, fracture initiation is also called 'burst' or 'rupture'.

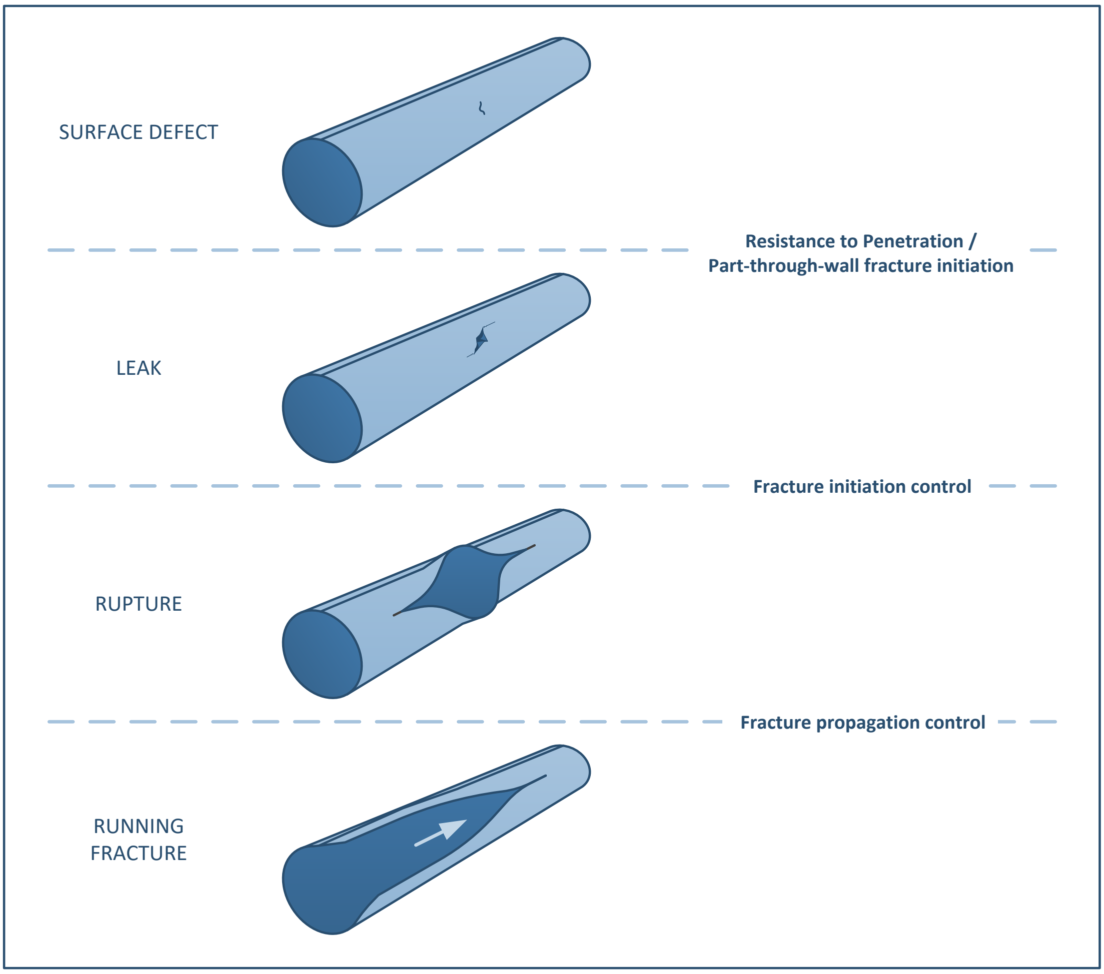

Figure 1-1: Four failure modes resulting from a longitudinal defect.

Controlling fracture is part of the safety management of a pipeline. Pipelines may be subject to a range of threats—including material defects, construction error, corrosion, mechanical damage, and cyclic fatigue—that can create cracks and similar sharp defects.

Fracture mechanics determines the conditions under which such defects cause a pipeline to fail and the way that it fails—whether it will leak or rupture, and how far the rupture will propagate (because steel pipelines are long continuous structures, a crack may propagate for a long distance if the conditions for fracture arrest are not met). The failure mode will in turn determine the consequence of failure—i.e. the safety risk to people in the vicinity, the duration of interruption to supply, and the harm to the environment. Additionally, the owner or operator may be exposed to financial, reputational and legal consequences.

To manage these consequences, the aim of a design that controls fracture is generally two-fold:

1) To limit the conditions under which a defect may lead to rupture.
2) To ensure that any defects that rupture will then arrest within an acceptable distance.

The material properties of the pipe that define how it will resist fracture are “strength” and “toughness”. Toughness measures the resistance of the material to a sharp crack; the tougher a pipe is, the amount of energy required to create a crack increases, and hence the greater is its tolerance to sharp defects. Toughness is different to strength, which measures the materials ability to resist stress in the absence of sharp cracks. The strength of a steel corresponds its grade (e.g. X70), and generally decreases as its temperature increases. In contrast, the toughness of structural steel, from which most pipelines are constructed, increases with temperature. Similarly, some mechanisms used to increase the strength of steel (such as work-hardening) will decrease the toughness. Consequently, the design of a pipeline steel must consider the optimum combination of both toughness *and* strength for the service.

Fracture control has simple objectives, but can be a complicated process, because of the many variables that it depends on. The demand for toughness will increase with diameter, stress and fluid compressibility (contained energy), and the properties that drive toughness are also a function of temperature.

For a small-diameter, thick-walled pipeline carrying a stable fluid at ambient temperatures, fracture control is simple because the demand for toughness is very low. At the other end of the scale, a large diameter, thin-wall, high-strength pipeline carrying carbon dioxide in arctic regions would be difficult to analyse and have very stringent toughness requirements.

Over the past few decades, technological development in the pipeline industry has created stronger and tougher steels, allowing pipelines to operate at higher pressures, with higher design factors, and with larger diameters.

However, it has become apparent in recent years that fracture control has not kept up in all areas. For some modern steels, the accepted models used for predicting fracture propagation are not performing well, which is likely a consequence of inadequacy of the toughness measurement methods used by the pipeline industry. Ongoing research is being conducted around the world to close this gap, so that industry can move forward in expanding the design envelope and can confidently continue to use modern steels safely.

1.1 Document purpose

The purpose of this Code of Practice is to aid designers and pipeline owners in controlling fracture of pipelines.

The first part of the document provides a theoretical background about fracture mechanics and materials. The second part of the document explains how this information is applied in the pipeline industry, and especially Australia. The performance requirements for a pipeline are defined, with reference to both safety management objectives and the mandatory requirements of AS/NZS 2885.1. Methods for achieving those requirements are presented in detail.

Extensive guidance is also included for how fracture control performance requirements can be applied retrospectively for “legacy” pipelines. These are pipelines that were designed under older editions of industry design codes, which did not address fracture control with the same understanding as is applied to new pipelines and may consequently be carrying a higher level of risk due to lower defect tolerance.

Finally, the appendices of this document provide some example of worked problems, statistics, and some information on fracture control for circumferential defects, which is especially relevant to station piping codes. The document does not cover the role of fracture mechanics in strain-based design.

1.2 Units

All equations used in this document are suitable for use with Standard International (S.I.) units as summarised in Table 1-1, except where explicitly noted otherwise.

Two alternate consistent systems (common in stress and fracture fields) are also provided below for reference. Note that $MPa.m^{1/2}$ for stress intensity factor is not included in any of these systems, because it is not a self-consistent set of units, though it is common in other codes.

Refer to Appendix A for definitions of nomenclature and acronyms used throughout this document.

Table 1-1 : Unit systems

Variable	S.I.	kN, mm	[1]N, mm
Distance	m	mm	mm
Mass	kg	kg	kg
Time	s	ms	
Temperature	K (or °C)	K	K
Velocity	$m.s^{-1}$	$m.s^{-1}$	
Force	N $(kg.m.s^{-2})$	kN	N
Pressure	Pa $(N.m^{-2})$	GPa	MPa $(N.mm^{-2})$
Energy	J (N.m)	J	N.mm
Power	W $(J.s^{-1})$	kW	
Torque	N.m	N.m	
Stress Intensity Factor	$Pa.m^{1/2}$	$GPa.mm^{1/2}$	$N.mm^{-3/2}$ $(MPa.mm^{1/2})$
Strain Energy Release Rate	$J.m^{-2}$	$J.mm^{-2}$	

[1] The N,mm system is not suitable for any variables involving time, because it is not consistent with the use of seconds (s) unless conversion factors are used.

Chapter 2 : An overview of fracture theory

Fracture mechanics is one of the youngest fields of materials science. It dates back to the 1920s, when a failure theory was developed by A. A. Griffith [1] who investigated the fracture of glass materials.

The landmark case that accelerated the greater proliferation of fracture mechanics was the Liberty-class ship failures. These ships were designed and built by the allied forces in World War II (1940s), and relied heavily on welded construction, rather than the riveted construction that had previously dominated that industry. The hull of the ship was now a continuous structure and so if a crack initiated, it could propagate from one panel to another across the entire ship.

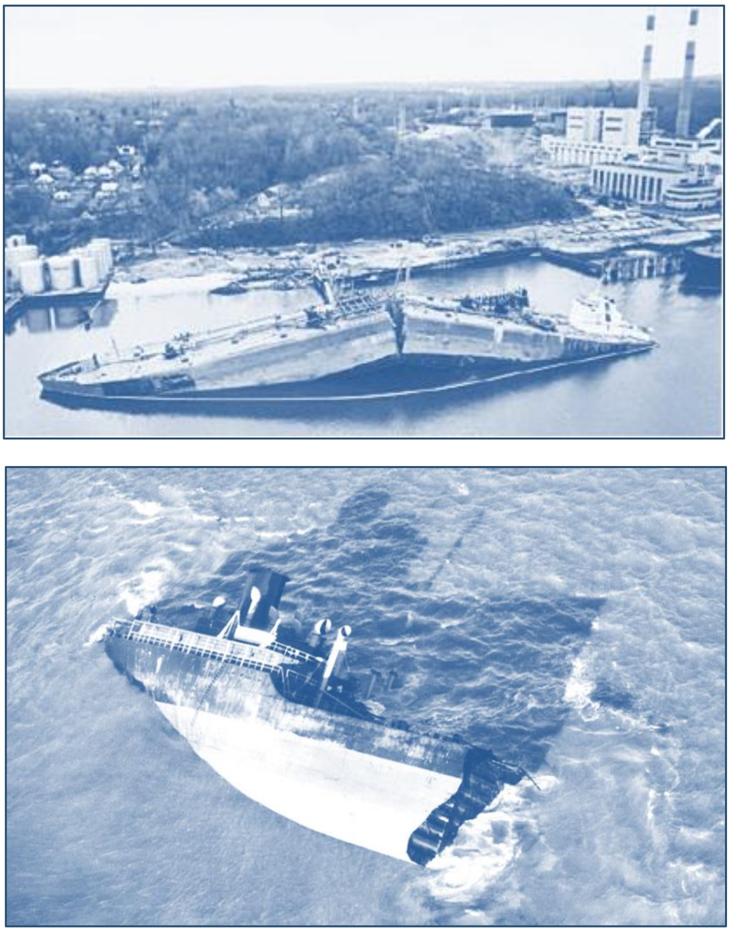

Figure 2-1: The catastrophic Liberty Ship tanker failures during cold weather were a landmark case that led to further development of fracture mechanics.

2,708 Liberty class ships were built between 1939 and 1945. 1031 incidents of brittle fracture had been reported by 1 April 1946, and were found to be originating in the square corners of hatches on the deck. In the most significant cases, the ships broke entirely into two. The SS Schenectady, shown in Figure 2-1 (top), broke during calm conditions in Portland harbour, due entirely to cold weather. These spectacular failures led to further investigation of fracture mechanics.

Today, consideration of fracture is a basic requirement for design in almost every industry.

Many textbooks have been written that provide details of how fracture is analysed and treated. This Chapter provides an overview of general fracture theory, highlighting theories and methods that are particularly used in the pipeline industry. *Fracture mechanics* by T. L Anderson [2] is a good source for any reader seeking a deeper level of understanding.

2.1 Solid mechanics

The question posed by fracture mechanics is, 'How does a solid behave when it has a sharp defect, or crack, in it?' The starting point to answering this is to understand how a solid behaves when there *aren't* any defects in it.

In any solid structure, forces between atoms in the structure act to hold it together and maintain its shape. When it is loaded and/or restrained, these forces respond, holding it together and transferring force through the structure between loads and restraints.

If an imaginary plane is considered, that divides a solid into two pieces, there would be force vectors acting between the two sides at each location on the plane's surface. The force on this plane (per unit area)[1] is described as a traction vector. A traction vector has three components: two parallel to the plane are called shear stress, and one perpendicular to the plane is called normal stress, which may either be tensile or compressive.

Because there are three independent (perpendicular) cross-sectional planes that can be considered, it takes three traction vectors to fully define the stress at a point. So, there are *nine* components of stress at any location[2], shown in Figure 2-3 (refer also Appendix E).

[1] 'Stress', 'strain' and 'traction', in this document, always refer to *true*, rather than *engineering* values, unless noted otherwise. Refer Section 4.1 for further detail.

[2] As also shown in Figure 2-3, corresponding pairs of shear components (co-planar) are equal in magnitude. This is a condition required for static equilibrium. Because three pairs are identical, it takes only six independent values to fully define the stress at a point.

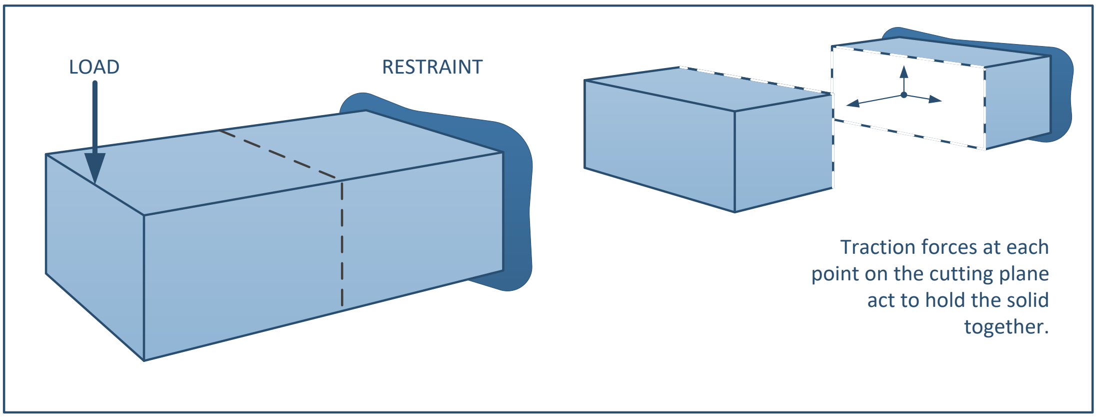

Figure 2-2: Traction forces in a solid.

As a solid is stressed in response to loading, it also strains. Where it is stretched, the atoms pull away from one another, where it is compressed, the atoms push towards one another, and where it is in shear, the atoms move anti-parallel to one another. Up to what is called the "yield" condition, the solid will return to its former shape once it is unloaded, which is described as elastic behaviour.

Above the yield point, plastic behaviour occurs—the atoms begin to slide over one another, many new "dislocations" are created in the material's crystal structure, and the solid will permanently change shape. Once the loading is severe enough, the solid will finally pull apart at what is called the "ultimate" condition.

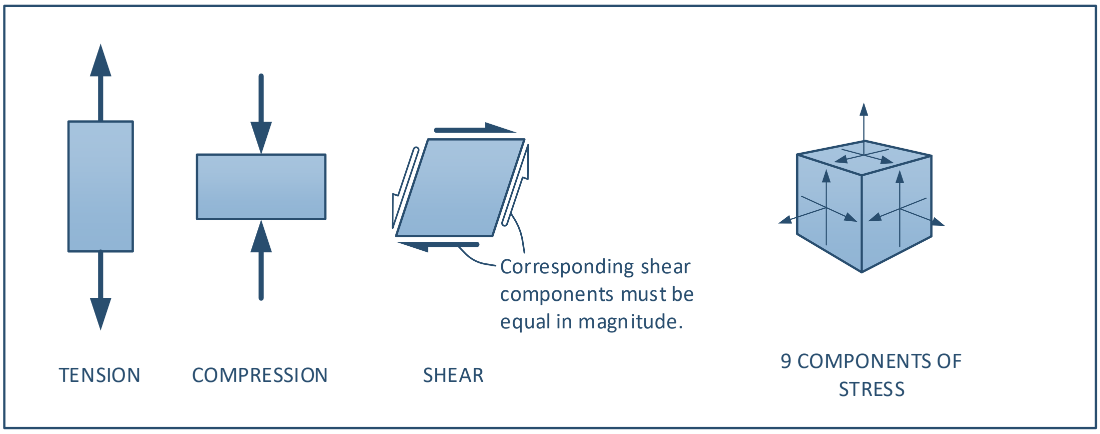

Figure 2-3: There are nine stress components required to fully define the stress at a point.

Stress analysis is used for the design of structures. Designers ensure that unacceptable yielding does not occur and the solid will not fail. However, stress analysis generally assumes that the structure has no flaws. In reality, structures can have a variety of flaws—including sharp flaws, or "cracks".

2.2 Stress at a defect

Where a structure has a crack in it, the only traction force that can be transmitted across the crack is compression. Any other components—tension, and the two shear components—cannot be transmitted across the crack.

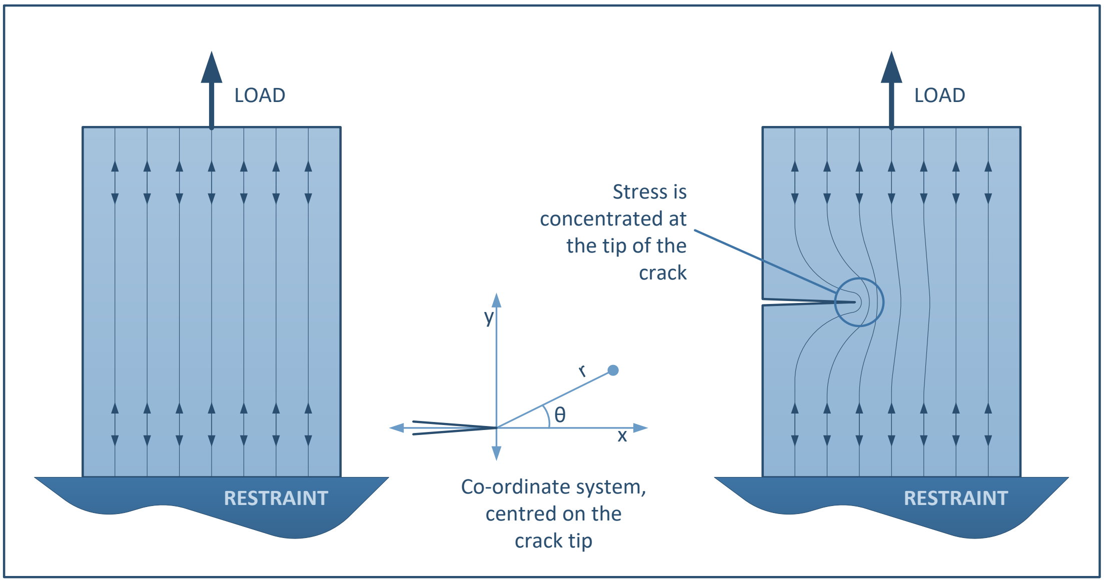

Figure 2-4: Stress concentration at the tip of a sharp crack.

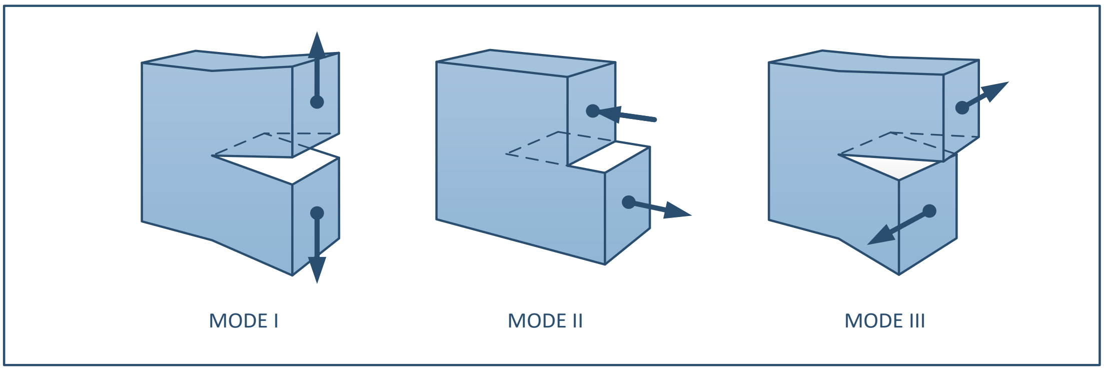

Figure 2-5: Three modes of loading on a sharp defect, corresponding to three components of traction on the fracture plane.

For the solid to take these loads, the internal forces are required to go *around* the crack as shown in Figure 2-4, and this causes stress to be concentrated along the crack tip.

There are three modes of loading that will cause stress concentration around a crack. These relate to the three components of the traction vector that could exist on that plane if the crack wasn't there. These are shown in Figure 2-5.

Around a crack, stress analysis gives a surprising and seemingly impossible result. A simple, elastic stress analysis will calculate that the stress distribution of tensile stress near a crack under Mode I (simple tensile) loading is:

$$\sigma_{yy} = \frac{K_I}{\sqrt{2\pi r}} + \text{(higher order terms)} \quad , \quad \theta = 0 \qquad \text{(2-1)}$$

In this equation, r is the distance from the tip of the crack as per the coordinate system in Figure 2-4. This result means that the tip of the sharp crack ($r = 0$) is a *singularity* in the stress field; the calculated stress at this location is *infinitely high*.

Initially, one would conclude that the solid must fail, because the calculated stress at the crack tip, being infinite, exceeds both the yield strength and the ultimate tensile strength of the material. However, there are at least three reasons why this is not the case:

1) A crack is not actually infinitely sharp. At most, the curvature of the crack tip must be similar to the distance between two atoms. In practice, the tip of a crack is always blunter than that, and as local yielding occurs, the tip becomes even blunter.

2) The continuity assumption breaks down at the scale of the crack tip. What happens at the tip of a crack is complicated, and occurs on a very small scale, so microscopic effects have an impact on the overall behaviour. Stress analysis assumes that both stress and strain are continuous fields. At the scale of atoms, this is not the case.

3) Yielding is caused by shear stress. The magnitude of the tensile stress is very high around the crack tip, but the overall stress state may still have very low shear components because of high triaxiality.[3] In simple terms, the material doesn't yield because, although the stress is high, it is being pulled equally in all directions. This is especially the case in plane strain conditions (refer Section 2.5.1).

The region around a crack tip is called the process zone, and what occurs in this region is complicated and cannot be understood purely from an understanding of yield and ultimate strength conditions. Another material property is required to determine whether a crack will grow or not.

[3] Stress analysts also use the terminology that the hydrostatic component of stress is high, but the deviatoric component is low, and it is deviatoric stress that directs the material to yield; refer Appendix E.

2.3 Characterising the crack tip

Experience indicates that solids can tolerate a degree of cracking without breaking completely and this tolerance is related to the size of the crack, the applied stress, and the material properties.

As shown above, the material response cannot be predicted using simple stress analysis or strength estimates; the onset of crack growth depends on complicated microscopic effects at the crack tip, governed by micro-structural material features which are the subjects of ongoing research.

Fracture mechanics does not attempt to model the region at the tip of the crack; the science of fracture mechanics is phenomenological, rather than derived from first principles. In order to predict the critical conditions in which a crack will or won't cause a failure, analysts use a variable called "fracture toughness" that can characterise the severity of loading and the material's response. There are several methods in use, derived from considerations of stress intensity, available energy, or deformation around the crack.

2.3.1 Stress Intensity Factor

The Stress Intensity Factor, K, is a commonly accepted approach to predict the behaviour of a crack[4].

For this method, the complicated behaviour in the process zone is characterised by the *severity* of the stress singularity predicted from an elastic stress analysis. The constant K_I in the stress distribution (Equation (2-1) above), is the Stress Intensity Factor for Mode I loading.

K is calculated for a given loading and geometry by the following formulae:

$$K_I = \lim_{r\to 0}\left(\sigma_{yy}\sqrt{2\pi r}\right) \qquad (2\text{-}2)$$

A simple case for fracture assessment is a crack of length 2c in an infinite plate. This has the following Stress Intensity Factor:

$$K_I = \sigma\sqrt{\pi c} \qquad (2\text{-}3)$$

(Note that in this formula, σ is the remote applied stress that would be present if the crack were not there; see Figure 2-4.) The Stress Intensity Factor has these properties: it A) increases with increasing crack length, B) is proportional to the stress in the solid, and C) relates to the geometry of the solid and the load.

[4] This should not be confused with SIFs used in piping stress analysis.

At some value, called the *critical* Stress Intensity Factor, K_{IC}, the crack will grow. Below this value, the crack will be stable. Hence, the criterion for fracture using this method is:

$$K \geq K_{IC} \tag{2-4}$$

This critical Stress Intensity Factor, K_{IC}, is a material property.

2.3.2 Strain energy release rate

Fracturing a material requires energy. Another way to express the condition for growth of a crack is that there is sufficient energy in the system to support formation of two new (crack) surfaces.

This is formalised in the Griffith crack growth criterion:

$$G \geq G_C \tag{2-5}$$

The potential energy of a loaded solid, Π, consists of two components: the strain energy in the solid minus the work done by external loads. A fracture will grow when the decrease in potential energy (per unit fracture growth) exceeds the energy required to create two new surfaces.

In a linear analysis, the change in potential energy per unit fracture area is called the Strain Energy Release Rate, G:

$$G = \frac{d\Pi}{dA_f} \tag{2-6}$$

The energy required to create the new fracture surfaces, per unit fracture area, is called the *critical* Strain Energy Release Rate, G_C.

This is calculated assuming elastic conditions and is mathematically relatable to the Stress Intensity Factor, K, by the following formulae[5] (E is modulus of elasticity, and ν is the Poisson ratio):

Plane stress: $$EG = K_I^2 \tag{2-7}$$

Plane strain: $$EG = (1 - \nu^2)K_I^2 \tag{2-8}$$

[5] These relations apply to a crack in pure Mode I loading. An advantage of the Strain Energy Release Rate is independence of loading mode. The mixed-mode relations are— for plane strain: $EG = (1 - \nu^2)(K_I^2 + K_{II}^2) + (1 + \nu)K_{III}^2$, and plane stress: $EG = K_I^2 + K_{II}^2 + (1 + \nu)K_{III}^2$.

2.3.3 Alternate variables for characterising crack growth

There are several other variables also used to characterise crack loading. These include:

- *Crack Tip Opening Displacement or CTOD, δ*. The Crack Tip Opening Displacement provides an estimate of the plastic deformation at the crack tip. It remains valid for more ductile materials which exhibit plasticity near the crack, and can be measured experimentally.
- *J-Integral,* J. The J-integral is the change in potential energy in the solid per unit crack extension. In a linear elastic material, this is identical to the Strain Energy Release Rate, G. However, the J-integral generalises the method so that it can also be applied when there is significant material plasticity involved, (used in EPFM, referred to later).

The general form of relationship linking the variables is[6]:

$$J \propto \sigma_Y \delta \propto \frac{K^2}{E} \tag{2-9}$$

In each case, the critical value at which the crack begins to propagate is treated as a material property representing toughness. Each variable has strengths and weaknesses in different contexts.

2.4 Plasticity and Failure assessment diagrams

The Stress Intensity Factor and Strain Energy Release Rate methods defined above are both used in what is called Linear Elastic Fracture Mechanics (LEFM). This method of fracture mechanics considers the loading on a crack as a function of the linear-elastic stress solution; it does not consider plastic deformation. Even though this solution is known to be in error near the crack, it is suitable for less ductile (brittle) materials, like glass, which will have a relatively small plastic zone at the crack tip.

In a material that is tougher, there will be a region near the crack tip that exceeds the yield condition, and plastically deforms. In this case, K and G are no longer effective at characterising the loading on the crack.

There are now *three* significant zones around the crack tip: the process zone, *the plastic zone*, and the elastic zone. In the plastic zone, the material undergoes yielding and plastically deforms, blunting the crack tip and changing the stress distribution.

[6] "∝" means "is proportional to"

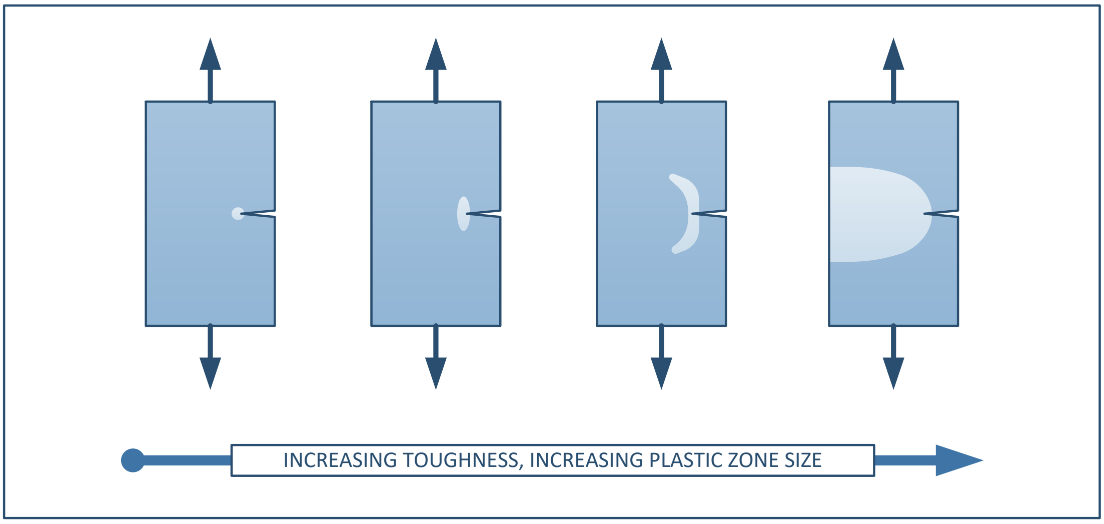

Figure 2-6: Increasing toughness implies an increase in the size of the plastic zone at the onset of failure.

2.4.1 Small scale yielding

If the plastic zone is small, linear elastic fracture mechanics can still be used, with a correction for small-scale yielding (LEFM-SSY). In this method, the elastic zone is called the K-dominant zone, because the stress distribution is still similar to the linear results (Equation (2-1)). Small-scale yielding methods require the application of a correction to LEFM that approximately represents the influence of a small plastic zone.

The Dugdale yield strip model is one method for estimating plastic zone size [3]. The method considers a loaded crack as the superposition of two different conditions: simple loading on a crack that extends to the edge of the plastic zone, and an opposing stress acting to close the crack within the plastic zone. The plastic zone is calculated such that the singularity at the edge of the plastic zone from these two scenarios cancels out.

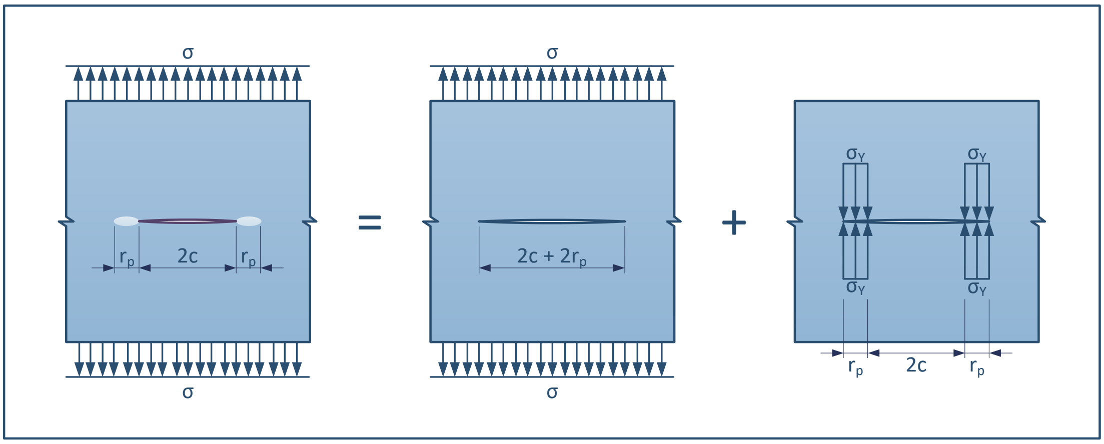

Figure 2-7: Dugdale yield strip method for accommodating a plastic zone of length r_p.

This method was used to derive the following formula for the plastic zone size:

$$\frac{c}{c + r_p} = \cos\left(\frac{\pi\sigma}{2\sigma_Y}\right) \tag{2-10}$$

A preliminary estimate of the crack growth criterion is to take an effective crack length (c_{eff}) to the edge of the plastic zone:

$$c_{eff} = c + r_p = \mathrm{c}/\cos\left(\frac{\pi\sigma}{2\sigma_Y}\right) \tag{2-11}$$

However, through a complicated derivation, Burdekin and Stone [4][7] derived a better estimate of the growth criterion—first using crack-tip opening displacement (CTOD, δ), and then transforming this back to an equivalent critical stress intensity factor per the relationship in Equation (2-9) above.

$$\delta = \frac{8}{\pi}\frac{c\sigma_Y}{E}\ln\left\{\sec\left(\frac{\pi\sigma}{2\sigma_Y}\right)\right\} \tag{2-12}$$

$$K_{eff} = \sigma_Y\sqrt{\pi c}\left[\frac{8}{\pi^2} ln\left\{sec\left(\frac{\pi\sigma}{2\sigma_Y}\right)\right\}\right] \tag{2-13}$$

In a brittle material, the failure stress and the plastic zone size are both small ($r_p \ll c$ and $\sigma \ll \sigma_Y$). In these conditions, the plastic zone size at failure approaches Equation (2-14).

$$r_p = \frac{\pi}{8}\left(\frac{K_I}{\sigma_Y}\right)^2 \tag{2-14}$$

2.4.2 Elastic-plastic fracture mechanics

As the plastic region becomes larger, the above methods are no longer viable. Other fracture mechanics analysis methods are required, such as Elastic-Plastic Fracture Mechanics (EPFM).

At high toughness, failure is eventually driven by plastic collapse. That is, the net stress reaches the ultimate tensile stress, and this causes failure.

2.4.3 Failure Assessment Diagrams

Failure conditions result from a combination of the stress state reaching either plastic collapse, fracture or a combination of these conditions.

Failure assessment diagrams are used to combine these aspects into a single failure envelope. On the vertical axis, a variable representing fracture severity, such as K, is plotted. The applied stress is plotted on the horizontal axis.

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[7] Refer also the work of Bilby *et al* [73] and [74].

One failure assessment diagram is based on the Dugdale method for estimating plastic zone size presented above, and has the following formula:

$$\left(\frac{\sigma}{\sigma_Y}\right)^2 = \left(\frac{K}{K_C}\right)^2 \times \frac{8}{\pi^2}\ln\left\{\sec\left(\frac{\sigma}{\sigma_Y}\frac{\pi}{2}\right)\right\} \tag{2-15}$$

This equation is graphed (using dimensionless variables) in Figure 2-8. Such diagrams can be used to determine which type of fracture analysis is most appropriate. At the left side of the graph, low-stress failures are driven by fracture criterion, $K = K_C$. Near the right of the graph, failure is predicted by a plastic collapse condition, when $\sigma = \sigma_Y$.

Other failure assessment diagram formulations exist, based on CTOD and J-integrals, which are not discussed here.

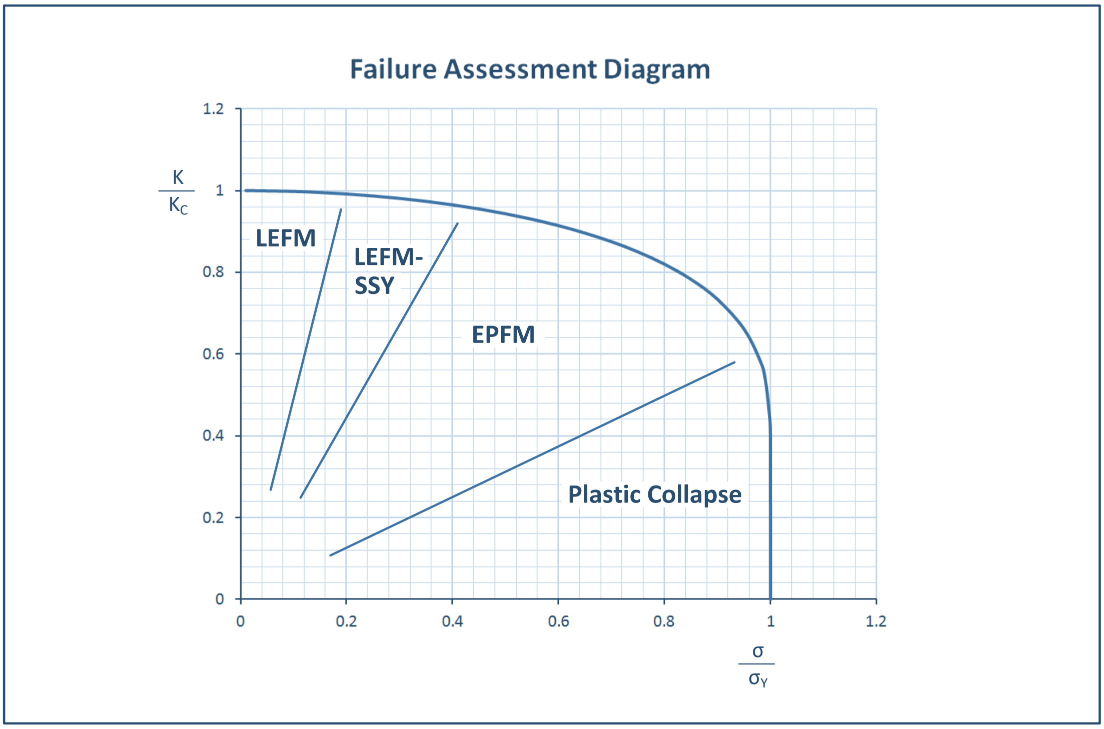


Figure 2-8: Dimensionless graph of the failure assessment diagram using Dugdale's model, assuming elastic-perfect-plastic material behaviour.

2.4.4 Flow stress

The plastic collapse condition used in the above theory ($\sigma = \sigma_Y$) uses an elastic-perfect-plastic (EPP) material model, where there is no strain-hardening after yielding. That means that failure occurs at the yield stress. Though this is mathematically convenient, it is not representative of actual materials. To improve the accuracy of this model, analysts substitute yield stress with flow stress, σ_f.

Ideally, the flow stress is single value of stress in the plastic zone that will provide equivalent overall behaviour to the actual stress distribution (which varies through the zone).

An arbitrary value is selected between the actual yield stress and ultimate tensile stress. It is common to approximate the flow stress as the linear average of the yield and ultimate tensile stress. At the design phase, the specified minimum yield strength (SMYS) and specified minimum tensile strength (SMTS) must be used, because actual material data is not yet available.

In Australia, it has been more common to estimate the flow stress as the Specified Minimum Yield Stress (SMYS) + 10 ksi, and this is the requirement of AS/NZS 2885.1.[8]

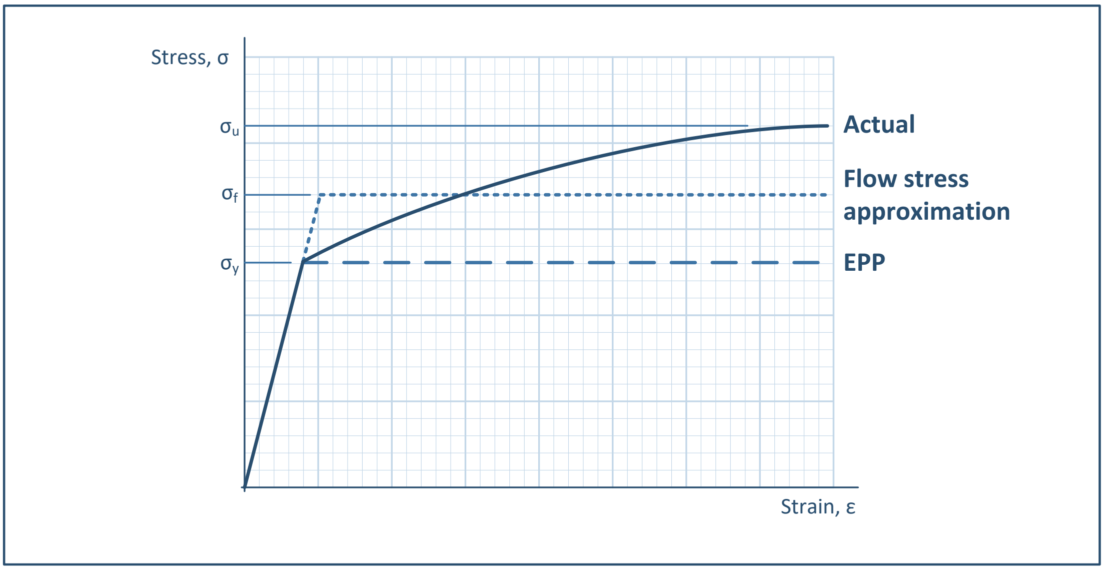


Figure 2-9: Comparison of tensile material models: actual behaviour, the idealised elastic-perfect-plastic (EPP) model, and flow stress approximation.

2.5 Material toughness

The toughness of a material relates to the energy required to fracture the material, per fracture surface area. Broadly, there are two types of fracture growth that can occur: brittle or ductile.

Brittle failure is characterised by "cleavage" fracture, which tends to travel through the material along crystal planes resulting in flat, reflective surfaces. In contrast, ductile failure involves void nucleation and growth ahead of the crack front, with the voids coalescing as the material between adjacent voids plastically deforms and finally separates. This leaves a rough, dull fracture surface.

[8] This is consistent also with API 579.

A brittle fracture is associated with low toughness. It requires relatively little energy to propagate and will travel at high velocities through a material. In contrast, ductile failures are associated with increased toughness and propagate at a lower velocity related to the absorption of more energy.

The fracture mode expected in steel depends on toughness, the material restraint state and temperature, which are explained below.

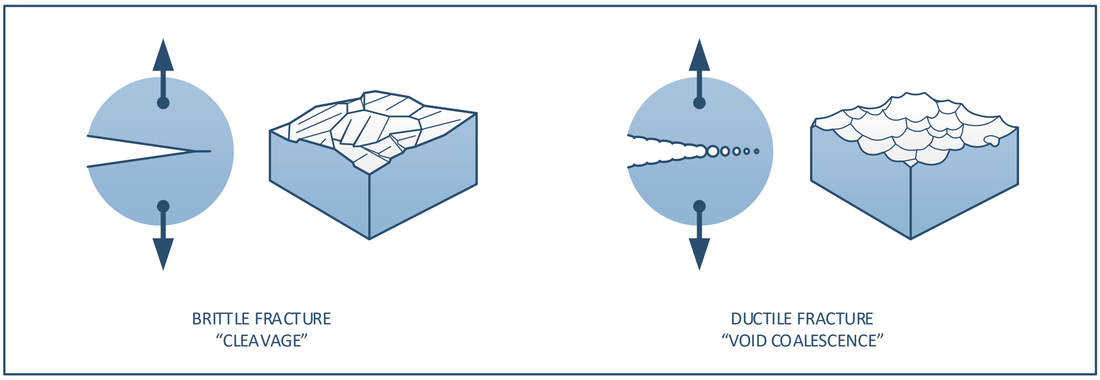


Figure 2-10: Two modes of fracture growth: brittle and ductile.

2.5.1 Restraint—thickness effect

In a thick plate subject to in-plane tension, there is a significant difference between the stress state on the surface of the plate and in the middle of the plate.

On the surface of the plate, there is no restraint preventing material from deflecting (and hence straining) perpendicular to the surface. Because the plate surface is free, the out-of-plane stress is zero. In these conditions, the triaxiality of the stress state is reduced, and the plastic zone is large. This is called *plane stress* conditions.

Away from the surface of a plate, the material is unable to deflect in the perpendicular plane, because the material around it resists deflection. This is called *plane strain* conditions. Under plane strain conditions, the stress state is more triaxial, and plasticity is inhibited.

In reality, plane stress conditions exist at the surface and the stress state becomes more and more like plane strain conditions towards the middle of the plate. In fracture experiments involving plates, a strip near the plate edge called "shear lips" exhibits ductile failure and is angled at around 45° to the surface (under Mode I loading). Towards the middle of the plate, the fracture surface is perpendicular to the plate and may be either brittle or ductile, depending on the material toughness. The size of the shear lips is a function of the material's ductility.

Thicker plates are more likely to fail in a brittle mode than thin plates, due to the increase in material restraint. On thin plates, the shear lips have an increasingly significant effect on the overall toughness exhibited by the plate.

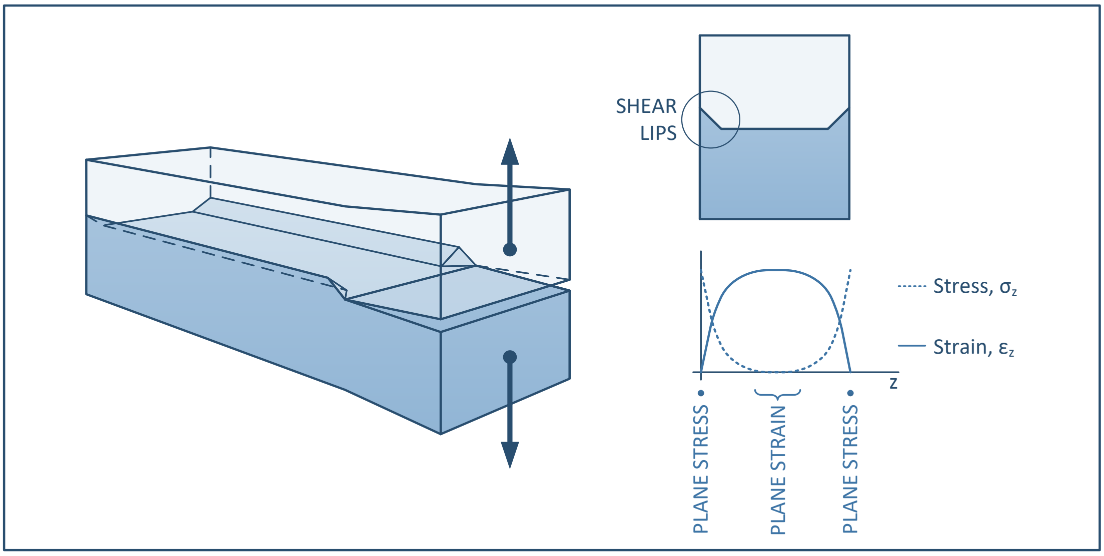


Figure 2-11: Formation of shear lips due to plane stress state on the plate surface.

2.5.2 Temperature—ductile/brittle transition

In many materials, such as carbon steels, material toughness is a temperature-dependent property. At low temperatures, steel exhibits low toughness (called lower-shelf toughness) and will break in a brittle, cleavage mode. As the temperature increases, at some point the steel will transition to higher toughness (called upper-shelf toughness), where it will break in a ductile mode. The transition occurs over a temperature range, within which the fracture surfaces will have mixed-mode fracture—some fibrous ductile appearance, and some cleavage fracture appearance.

Typical toughness-temperature curves showing this effect are provided in Figure 2-12.

Though the toughness transition occurs over a temperature range, a specific Ductile-Brittle Transition Temperature (DBTT) is nominated. There are several ways that this can be defined. One definition is the temperature at which the toughness is half-way between the lower and upper shelf ($T_{50\%}$).

A method more common in pipeline industry is to analyse the appearance of the fracture surface of test specimens. The transition temperature is defined as the temperature at which 85% of the fracture surface has failed in a ductile mode, ($T_{85\%}$) [5]. This is also called the fracture appearance transition temperature (FATT). When upper-shelf toughness is required, and hence it is important to remain above the transition temperature, this definition is preferred and is more conservative.

The transition temperature is also dependent on material thickness; a thinner material will have a lower transition temperature than a thick one. Where less restraint allows for more plane-strain behaviour, it takes a reduced temperature to cause brittle behaviour. (This is an important consideration when the specimen size for testing is selected).

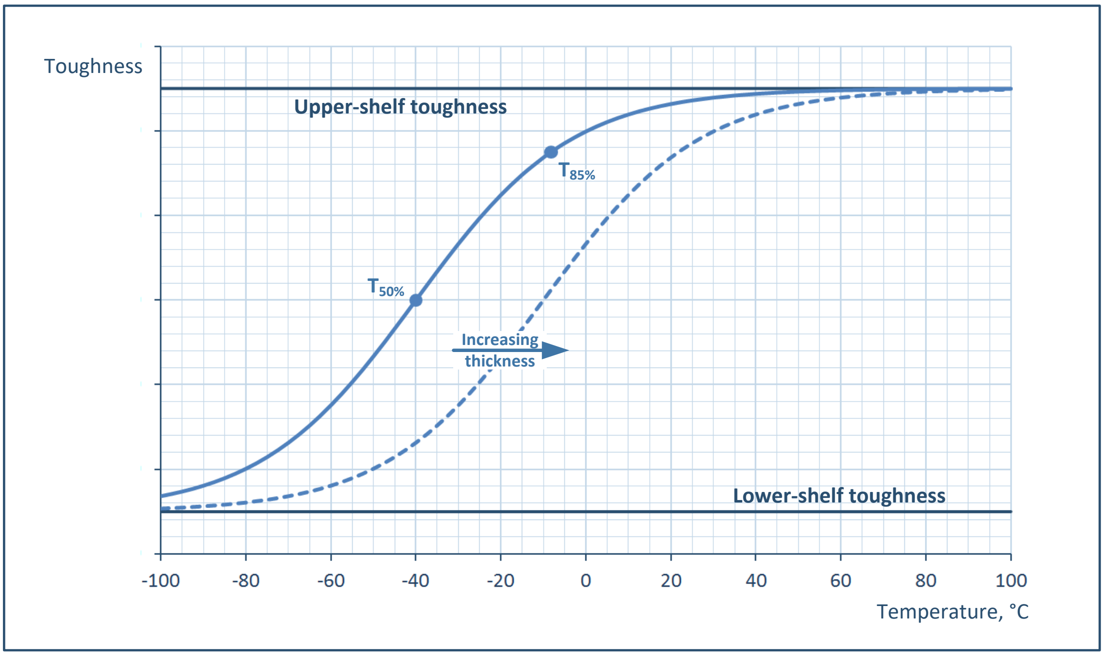


Figure 2-12: Typical toughness-temperature relationships.

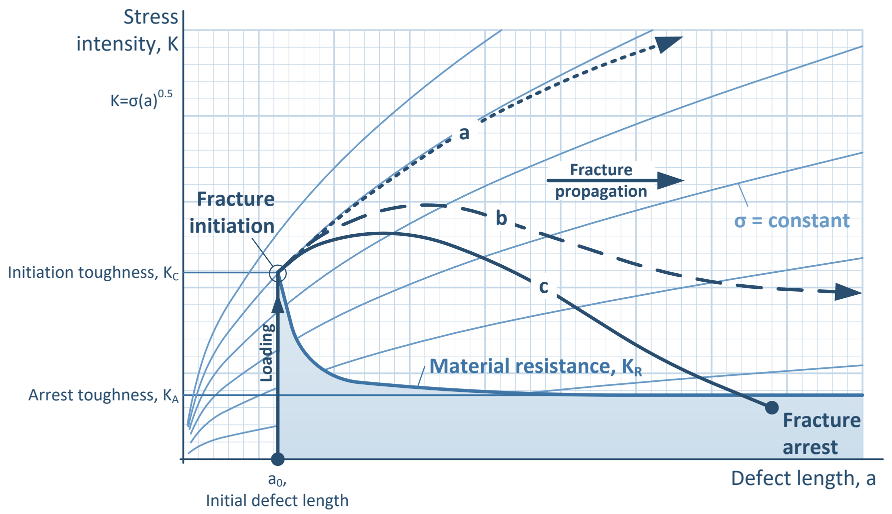
2.6 Fracture growth

After initiation of a crack, propagation depends on two factors: A) what happens to the load; and, B) what is the material's resistance to crack growth.

Item (A) depends on what is causing the load. Initially, the severity of the load on the crack is likely to increase because the crack becomes longer, which increases the stress intensity. However, the load on the crack may subsequently decrease with crack growth, for a range of reasons. The load may be relieved by displacement around the crack or transferred elsewhere in the structure. In the case of a pipeline, the fluid leaks through the crack and relieves internal pressure in the pipe. There will be a finite amount of energy available in the system, and the crack will stop growing once the available energy is depleted.

With regard to item (B), the resistance of a material to crack propagation is less than its resistance to crack initiation. In a sense, fracture growth behaves similarly to friction; a surface provides less frictional resistance to an object that is already sliding than to an object that is stationary. Similarly, a material provides less resistance to a crack that is already growing, than to a crack that is not.

The dynamic toughness[9] of a material also has a different transition temperature to the static toughness. Research in the 1960s to 1980s found that the fracture initiation transition temperature (FITT) was at least 30°C below the fracture propagation transition temperature (FPTT) for pipeline materials [6] [7], but this correlation cannot be relied on for modern materials [8] [9].



After a fracture initiates, the material resistance drops off to the arrest toughness (dynamic toughness) and the fracture propagates. Three examples shown are: (a) grows rapidly under sustained stress, (b) sees a decrease in stress but doesn't arrest, (c) sees a decrease in stress that unloads the crack, which arrests

Figure 2-13: Dynamic crack growth.

These two effects are illustrated in Figure 2-13. It is worth noting that the margin between the stress intensity on the crack (K) and the material resistance (K_R) determines the fracture velocity. The difference between the two can be related to the excess amount of energy above that needed to create the fracture surfaces. Some of the energy will be released as kinetic energy as the two sides break apart. If the margin of excess energy is high, the crack

[9] There are two types of dynamic fracture mechanics. The first is a rapidly changing load, such as "impact" toughness when a crack is loaded suddenly; the second is a rapidly growing crack, which is under consideration here. The two are related and both exhibit less material resistance and higher transition temperatures than static loading.

may split into two or more branches (as is frequently observed in brittle materials like glass, which “shatter”).

Cracks initiated under Mode II or III loading will typically re-orient over some distance to remove shear loading and be in pure Mode I loading by propagating perpendicular to a principal stress.

Chapter 3 : Pipeline fracture theory – longitudinal defects

Over several decades, much research has been done into the role of fracture mechanics in the pipeline industry. Theories and design methods have been developed to define, measure and control fracture in pressurised pipelines. Chapter 2 presented an overview of general fracture mechanics theory. This Chapter provides detail regarding how this is applied to the pipeline industry in analysing longitudinal defects (that is, defects orientated parallel to the pipe axis, refer Figure 1-1).

3.1 Stress due to pressure

The loading on a longitudinal defect in a pipe is from the large tensile hoop stress that results from internal pressure[1]. This loading is very similar to planar tensile stress in an infinite plate, except that the pipe is able to bulge (deflect radially) due to the crack in it, which increases the stress concentration.

The exact formula for hoop stress due to pressure, at radius R in the pipe wall, is provided by Lame's equations [10]:

$$\sigma_h = p\frac{D_i^2}{D^2 - D_i^2}\left(1 + \frac{D^2}{4R^2}\right) \tag{3-1}$$

The average hoop stress through the pipe wall thickness is given by:

$$\sigma_h = \frac{pD_i}{2t} \tag{3-2}$$

This equation (3-2) is called the Barlow equation. For relatively thin pipes (D/t > 20), it is a good single approximation, as the hoop stress is near uniform through the pipe wall. It is common to conservatively use the outer diameter rather than the inner diameter. This has the dual benefit of overestimating the principle stress (above what will be seen on the inner wall of the pipe), and it is

[1] Torsional stress, ring bending/ovalisation (e.g. from vehicle crossings), and some other load types can also apply stress on a longitudinal defect, but in pressure pipelines, these may safely be neglected because the pressure load is much greater in magnitude.

approximately the formula for the Tresca stress, used to predict the yield condition.[2]

In Australian high pressure pipelines, the maximum hoop stress due to pressure, as determined from the Barlow equation using the outer diameter, is permitted to be as high as 80% of the material's Specified Minimum Yield Stress (SMYS), though most pipelines are designed with a lower design factor.

3.2 Longitudinal fracture initiation

Fracture in a pipeline occurs when a longitudinal sharp defect exceeds the Critical Defect Length (CDL) of the pipeline. Formulas in this section use the half-defect length, c, which is related to the CDL at critical conditions by Equation (3-3):

$$CDL = 2c \tag{3-3}$$

3.2.1 The NG-18 Equation

The Battelle Memorial Institute, under the direction of the NG-18 line-pipe committee of PRCI, developed an equation that is commonly used for calculating the CDL [5]. This equation is derived from the yield strip theory (also used in Section 2.4.3).

$$K_{IC}^2 = \frac{8c\sigma_f^2}{\pi} \ln\left\{\sec\left(\frac{\pi M_T \sigma_h}{2\sigma_f}\right)\right\} \tag{3-4}$$

The critical stress intensity factor is intended to be for plane-stress conditions, so Equation (2-7) is used to convert this into energy terms where Charpy results have been used to determine toughness.

A pipeline's curved surface is able to bulge due to internal pressure, which amplifies the load above that experienced by a flat plate in tension. NG-18 used the Folias correction factor, M_T, to account for this geometry and the stress-amplifying effect of pipe bulging around the crack [11] [12] [13]:

$$M_T = \left(1 + 2.51\frac{c^2}{Dt} - 0.054\frac{c^4}{D^2t^2}\right)^{0.5}, \qquad \frac{c^2}{Dt} < 8 \tag{3-5}$$

This equation is only valid for axial cracks up to around 1.2 times the pipe radius in length. Other equations exist for the Folias factor for longer cracks.

[2] AS/NZS 2885.1 uses the external diameter Barlow formula with the nominated design factor to determine the "required" wall thickness, and it is also used in API 5L to calculated the hydrostatic test pressure in the mill.

It can be seen in Equation (3-4), that as the toughness increases ($K_C \rightarrow \infty$), the plastic collapse criterion approaches the following:

$$\frac{\sigma_h M_T}{\sigma_f} = 1 \tag{3-6}$$

Consequently, this equation (3-6) can be used to determine the limiting high-toughness Critical Defect Length (CDL_h) for the pressure and wall thickness under consideration.

Rearranged, the equation for the limiting CDL_h, in terms of c, is:

$$c^2 = Dt\left[23.241 + \sqrt{558.65 - 18.519\left(\frac{\sigma_f}{\sigma_h}\right)^2}\right] \tag{3-7}$$

3.2.2 Part-through-wall defects

Through-wall defects can form when surface defects (part-through-wall defects) grow through the pipe wall. This occurs when the part-through-wall defect exceeds a critical depth. This does not necessarily imply that the crack will then rupture, as the resulting through-wall crack may be shorter than the CDL.

Equations are also provided to predict when a part-through-wall defect will grow through the pipe wall. The following equation is used. It is analogous to the above equations for a through-wall defect,

$$K_{IC}^2 = \frac{8c\sigma_f^2}{\pi}\ln\left\{\sec\left(\frac{\pi M_P \sigma_h}{2\sigma_f}\right)\right\} \tag{3-8}$$

The bulging factor for a part-through-wall defect is M_P, and is calculated from the Folias factor for a through-wall defect:

$$M_P = \frac{M_T t - d}{M_T(t-d)} \tag{3-9}$$

Here d is the depth of a rectangular part-through-wall crack. This equation is known to be conservative. For a high toughness material, the limiting plastic collapse condition is predicted by:

$$\frac{\sigma_h M_P}{\sigma_f} = 1 \tag{3-10}$$

This can be rearranged to:

$$M_T = \frac{\sigma_h d}{\sigma_h t - \sigma_f(t-d)} \tag{3-11}$$

It is common to develop a series of curves for part-wall defects, to determine which surface defects are expected to result in a leak, and which are expected to result in a rupture. An example of these curves is shown in Figure 3-1.

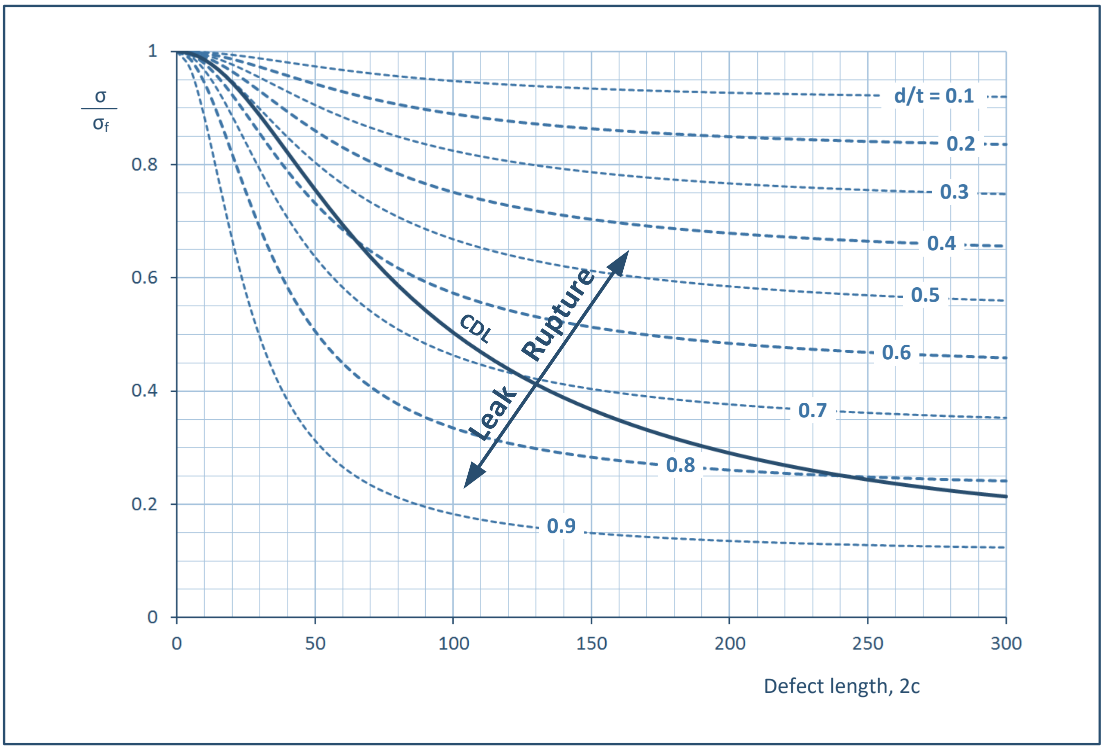


Figure 3-1: Critical part-through-wall defects may cause a leak or rupture, depending on their length compared to the Critical Defect Length for a through-wall crack.

3.2.3 Stress intensity factor solutions

Methods to calculate the stress intensity factor and reference stress for geometrically simplified crack-like defects in cylinders are provided in published literature and most notably in API 579 and BS 7910. These can be evaluated and compared to failure conditions by applying the failure assessment diagrams from the same standards.

One difficulty with these methods is that it is not easy to apply them using Charpy V-notch (CVN) toughness testing results, and yet CVN toughness is the most readily available material data relating to toughness. If alternate test methods have been applied (obtaining critical K_I or J values, for instance), then these stress intensity factor calculations can be applied more readily. Otherwise, a conservative conversion may be warranted, as discussed in Section 4.2.1.1.

3.3 Calculating brittle fracture propagation conditions

Brittle fracture propagation may occur below a material's fracture propagation transition temperature (FPTT), as shown in Figure 3-2. For most pipelines, the design intent is to prevent this by ensuring the material is operating above the FPTT. However, theory is also provided to determine when there is or isn't sufficient energy to propagate a brittle fracture.

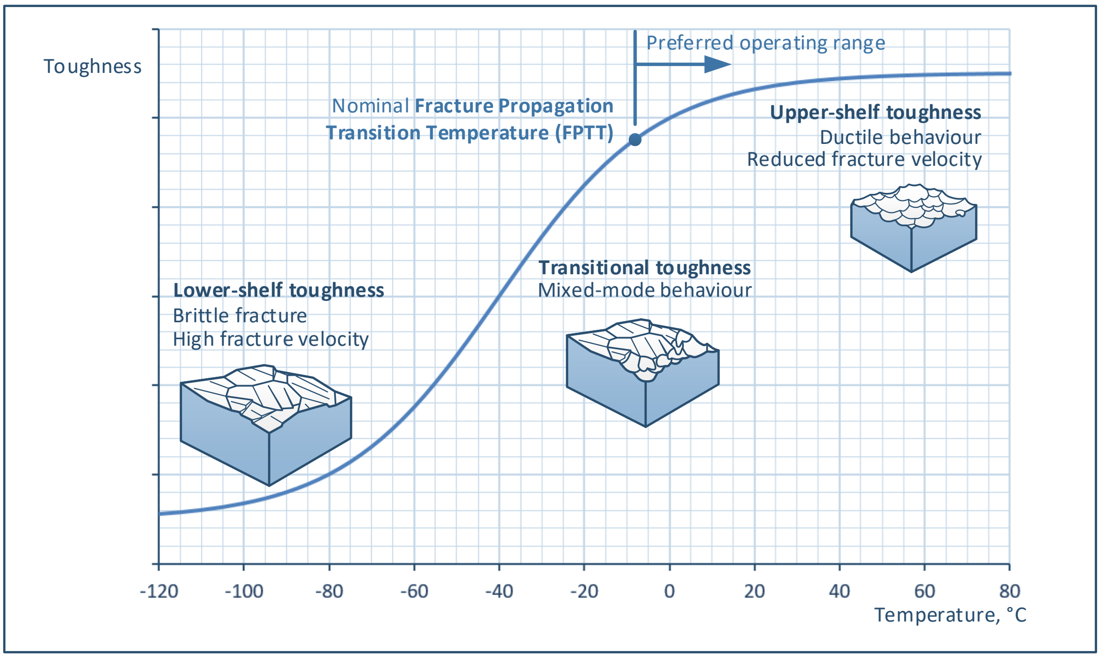


Figure 3-2: Fracture propagation transition temperature.

In a pipeline, the energy required to fracture the pipe comes from the energy of compression in the fluid and the elastic strain energy stored in the pipe steel (due to hoop stress from internal pressure).

3.3.1 Brittle fracture

Brittle fractures have comparatively low energy demand, and extend at very high speeds (around 450 to 900 m/s) – greater than the acoustic velocity of a gas (which is around 350 to 450 m/s for natural gas). Due to the rapid and small displacement of the steel as the crack propagates, brittle fracture arrest theory for gas pipelines assumes that the energy driving the fracture is provided entirely by the elastic strain energy in the pipe and that the crack grows too fast for the energy of gas decompression to contribute.

A condition for brittle fracture arrest can be determined by equating this available energy with the *critical* Strain Energy Release Rate, G_C, which is the energy required to propagate the fracture (i.e. form two new surfaces) [14].

The strain energy per unit of pipe cross-sectional area is calculated as the area under the stress-strain curve, multiplied by the pipe circumference:

$$G = \frac{\pi \sigma_h^2 R}{E} \left(= \frac{\pi P^2 R^3}{E t^2} \right) \tag{3-12}$$

This is compared to the lower-shelf critical energy release rate, G_C, to predict the minimum conditions for brittle fracture propagation. G_C can be approximated from Charpy test results, using Equation (4-3) and relevant corrections (see Section 4.2.1). Brittle fracture theory is commonly used to determine threshold hoop stress for brittle fracture propagation, σ_{BF}, at *lower-shelf* toughness. For this purpose, the Charpy toughness of the steel at 0% shear area can be used, as in the following rearrangement of Equation (3-12).

$$\sigma_{BC} = \left(\frac{[C_v]_{S_v=0\%}}{A_v}\frac{E}{\pi R}\right)^{1/2} \tag{3-13}$$

The lower-shelf toughness may also be approximated from other Charpy test data points using Equation (4-8). This assumes that the lower-shelf toughness is 10% of the upper-shelf Charpy toughness. It is recommended that a safety factor of 2 be used in conjunction with that method to accommodate uncertainty.[3]

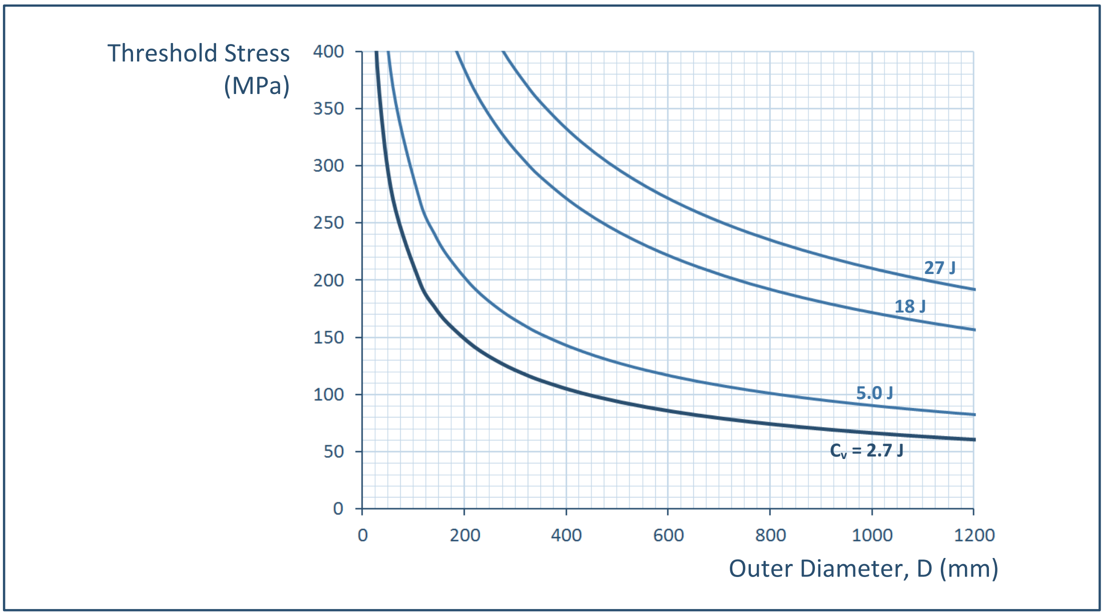


Figure 3-3: Threshold stress for brittle fracture vs. outer diameter for various Charpy toughness values.

Alternately, Equation (3-12) can be modified to include the strain energy of the fluid inside the pipe, with:

$$G = \frac{\pi P^2 R^3}{E t^2} + \frac{\pi P^2 R^2}{2Bt} \tag{3-14}$$

[3] Alternatively, in the absence of any data, Australian Standard AS/NZS 3788 permits an assumption that K_{IC} = 40 MPa.m$^{1/2}$, which equates to a strain energy release rate of G_C = 8,000 J/m^2—this would be conservative for any pipeline steel.

Here B is the bulk modulus of the fluid. This equation accounts for all the energy present in the system, so is an upper bound solution to the problem.

Comparison of Figure 3-3 and Figure 3-4 shows the effect of the second term in this equation. In this instance, the properties of water were applied and caused a reduction in the calculated threshold stress.

This second equation could be used for liquid pipelines, in which the strain energy release from liquid decompression can contribute meaningfully to the fracture and may be exhausted through the displacement that occurs as it fractures. It is not reasonable for a gas pipeline to use this approach, because the gas expands over a larger volume, containing a significant amount of energy that cannot contribute to the fracture over the displacement involved.

Both approaches include significant assumptions regarding what energy can contribute to the fracture; more detailed modelling would be possible when the results are unacceptable, but Equation (3-12) has been in use for many years and the output from it is likely to be conservative.

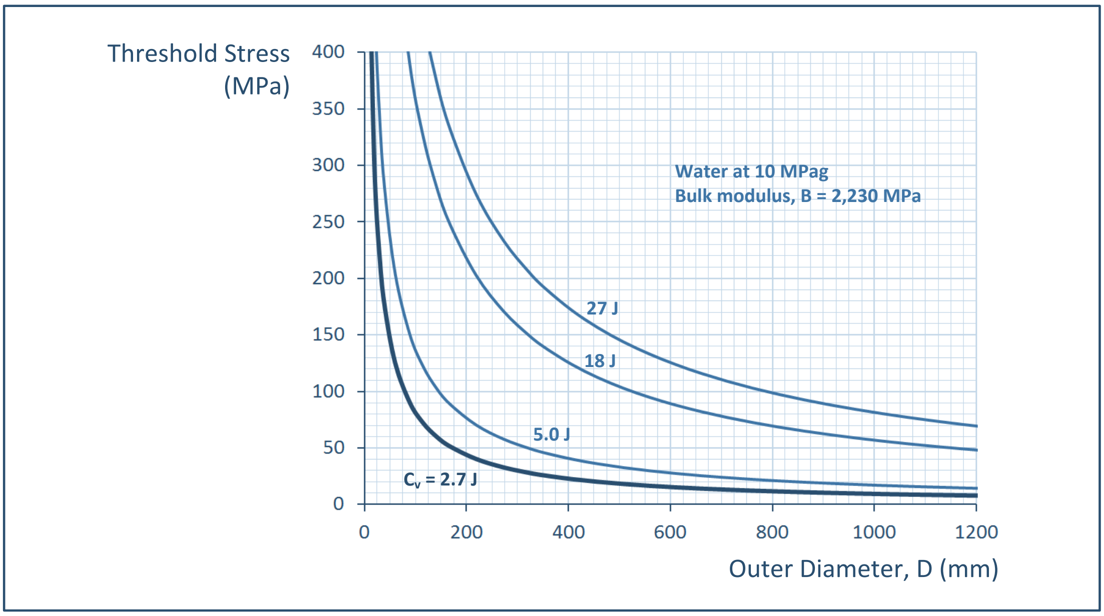


Figure 3-4: Example of a conservative estimate of minimum threshold stress for brittle fracture including strain energy of water at 10 MPag.

3.4 Calculating ductile fracture propagation conditions

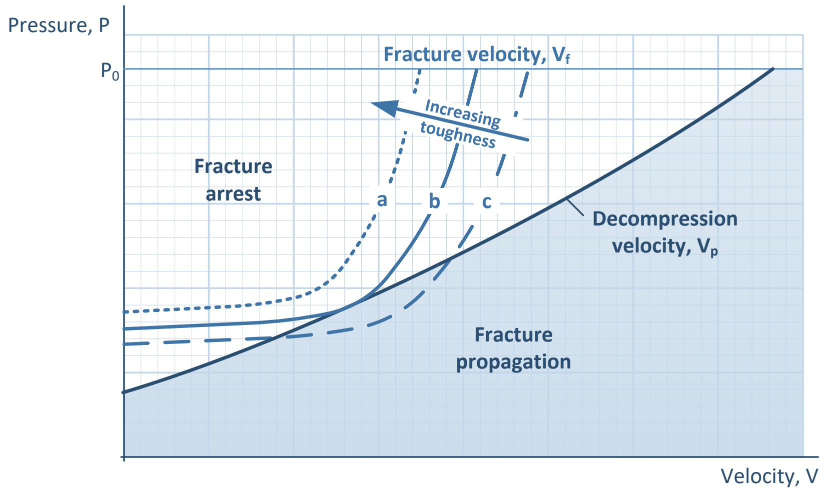
In gas pipelines, the decompression process is slow due to expansion of the gas. Ductile cracks grow in the order of 100 to 300 m/s, so even a ductile-mode fracture may be able to propagate indefinitely, if the velocity of the fracture is greater than the velocity of the gas decompression wave. Generally, the principle for preventing a propagating failure is to slow cracks down.

A generalised method for predicting whether a fracture will arrest or propagate was developed by the Battelle Institute. It is called the Battelle Two-Curve Method (BTCM) [15].

In the BTCM, two equations are developed.

1. *The relation between fracture velocity and hoop stress.* Below the arrest stress, the fracture will not propagate. Above the arrest stress, the speed of fracture will increase with increasing stress.
2. *The relation between decompression wave-speed and pressure.* At the tip of the crack, gas rapidly leaves the pipeline, typically at supersonic velocities. As gas flows toward the rupture, a decompression wavefront travels down the pipeline, causing a pressure gradient that varies from operating pressure far down the pipeline, decreasing to a stable pressure at the rupture (flow at the rupture is choked).

The criterion for a ductile fracture to arrest is that the decompression wave-speed must travel faster than the crack so that the stress on the crack reduces below the arrest stress. This is achieved if the gas decompression curve is below and does not intersect with the fracture velocity curve (see Figure 3-5).



Of the curves shown: (a) will result in fracture arrest, (b) is the boundary condition between arrest and propagation, used to determine the "arrest toughness", and (c) will result in unstable fracture propagation.

Figure 3-5: The two-curve model compares facture velocity and decompression velocity curve.

3.4.1 Fracture velocity equation

Battelle also developed fracture velocity formulae, related to the Charpy toughness. This method is still in common use, however, application at high Charpy toughness values is known to produce inaccurate results, as discussed below.

The arrest stress is calculated based on the NG-18 fracture equation (Equation (3-4)), in which the Folias factor, M_T, is taken as 3.33, and the effective fracture length, c_{eff}, is taken as $3\sqrt{Rt}$, resulting in Equation (3-15) [16].

$$\sigma_a = \frac{2\sigma_f}{3.33\pi}\cos^{-1}\left\{\exp\left(\frac{-\pi G_D E}{24\sigma_f^2\sqrt{Rt}}\right)\right\} \tag{3-15}$$

The material resistance is represented by the critical dynamic Strain Energy Release Rate, G_D.[4] This is estimated from Charpy tests, using Equation (4-3) with correction for specimen thickness as required.

From Barlow's formula for hoop stress, Equation (3-2), the arrest stress can be converted to an arrest pressure:

$$P_a = \frac{2\sigma_a t}{D_i} \tag{3-16}$$

The speed of a fracture is limited by the speed of the slowest process involved in creating the new fracture surface. In the case of a ductile fracture, the velocity of the crack front is assumed to be limited by the propagation speed of the plastic zone ahead of the crack, V_{pl}.

The fracture velocity is also related to the ratio between the pressure at the crack tip, and the arrest pressure. This is assumed to be a power-relationship, according to the following formula [5] [15]:

$$V_f \propto V_{pl}\left(\frac{P}{P_a} - 1\right)^x \tag{3-17}$$

The limiting propagation speed of the plastic zone ahead of the crack is the speed of a plastic wave of a *characteristic* plastic strain.[5]

$$V_{pl} \propto \frac{\sigma_f}{\sqrt{G_D}} \tag{3-18}$$

The constant of proportionality, C, and the exponent, x, were determined experimentally by Battelle. The fracture velocity is consequently represented by the following equation [5]:

$$V_f = C\frac{\sigma_f}{\sqrt{G_D}}\left(\frac{P}{P_a} - 1\right)^{1/6} \tag{3-19}$$

[4] Other reports use R for the resistance parameter. G_D has been used in this report, because it is equivalent to the strain energy release rate in dynamic conditions.

[5] In the derivation by Leis and Eiber [5], it was assumed that the critical dynamic Strain Energy Release Rate, G_D, is proportional to the area under the stress-strain curve at the characteristic strain. The velocity is proportional to the true stress-strain curve slope at that location – which was assumed to take the form of a power-function.

Where C is a constant taken as 2.75 x 10^{-4} for soil backfill and 3.79 x 10^{-4} when no backfill is used (based on S.I. units).

3.4.2 Limiting case

The maximum value for an inverse cosine function, as used in Equation (3-15), is π/2. Because of this, the maximum value of the arrest stress can be calculated as:

$$\sigma_{a,max} = \frac{2\sigma_f}{3.33\pi}\frac{\pi}{2} \approx 0.3\sigma_f \tag{3-20}$$

Pipes with high toughness will approach this value, as will pipes that have small diameter and thickness, as can be seen in Figure 3-6 below (this graph uses flow stress of SMYS + 10 ksi).

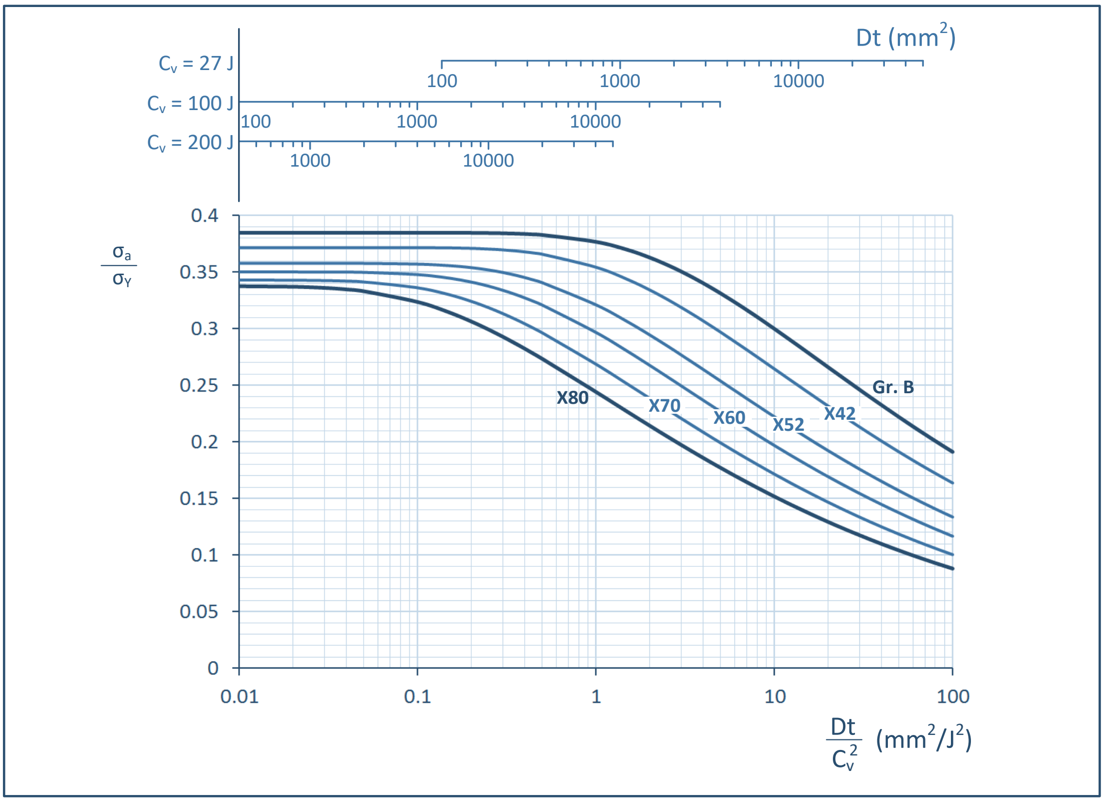


Figure 3-6: Ratio of arrest toughness to yield strength for various pipe sizes.

A propagating fracture is consequently difficult to sustain when the operating stress is less than about 40% of the yield strength, and consequently it is used in some codes as a stress level below which it is not necessary to consider fracture propagation.

Theory supports that fracture *initiation* is unlikely in this range also. Thirty per cent of SMYS (i.e. a "design factor" of 0.3) is used in some codes as a stress level below which it is assumes that ruptures will not occur, because a defect would have to be very long to fail at this stress level.

3.4.3 Correction models

The fracture velocity model developed by Battelle is known to be incorrect at higher toughness values and for high-strength materials (e.g. X65 and higher). There are a range of potential factors contribute to the break-down of the method, some of which include:

1) Controversy over the best estimate for flow stress, which is either taken as the average of SMYS and SMTS or SMYS + 10 ksi, which approaches some high strength material's specified minimum tensile strength. The two-curve model is empirical and was developed using the latter of these two assumptions, it was not validated using higher strength steels.

2) The formula is a function only of flow-stress, dynamic toughness (G_D determined from Charpy testing) and thickness. In reality, there are reasons why the material's ductility (as estimated by yield-to-tensile strength ratio, uniform elongation etc.) and other properties will be relevant to the formation and velocity of the plastic wave ahead of the crack.

3) The Charpy test method is less adequate for high-toughness materials for which a disproportionate amount of energy is absorbed in plastic deformation rather than crack propagation.

   The predicted arrest toughness also becomes non-conservative because of the small-scale nature of the test and the difference in loading conditions between the test and the ductile fracture of a pipeline. In ductile materials, the plastic zone is very large compared to a Charpy specimen that is 10mm deep.

4) The bulging factor is based on geometric assumptions about how the material deflects at the tip of a crack, but the actual deformed shape may be different.

Considerable effort has been and continues to be made by researchers worldwide to reduce uncertainty in the fracture velocity estimations. There are a number of correction models available to industry, intended to accommodate error in the arrest toughness calculation, shown in Table 3-1 and Figure 3-7.

The correction models currently recommended by AS/NZS 2885.1 are listed in Section 6.3.3. Note that for X80 materials in general, Australian Standards recommend a multiplication factor of 1.4x, due to uncertainty. Note also that the European Pipeline Research Group (EPRG) set mandatory Charpy toughness values up to 200 J, and composition formulae to define a "lean gas".

Table 3-1 : BTCM method correction models

Model	Formula	
Leis [17]	$C_{v,cor} = C_v$	$\Leftarrow C_v \leq 95J$
	$C_{v,cor} = C_v + 0.002C_v^{2.04} - 21.18$	$\Leftarrow C_v > 95J$
Eiber 2008 [18]	$C_{v,cor} = C_v + 0.003C_v^{2.04} - 21.18$	$\Leftarrow \sigma_Y \geq 555MPa$
Wilkowsky 1977 [19]	$C_{v,cor} = 0.056(0.102C_v + 10.29)^{2.597} - 16.81$	
Wilkowsky 2000 [20]	$C_{v,cor} = 0.043(0.102C_v + 10.29)^{2.597} - 16.81$	$\Leftarrow \sigma_Y \leq 485MPa$
	$C_{v,cor} = 0.056(0.102C_v + 10.29)^{2.597} - 16.81$	$\Leftarrow \sigma_Y \geq 555MPa$

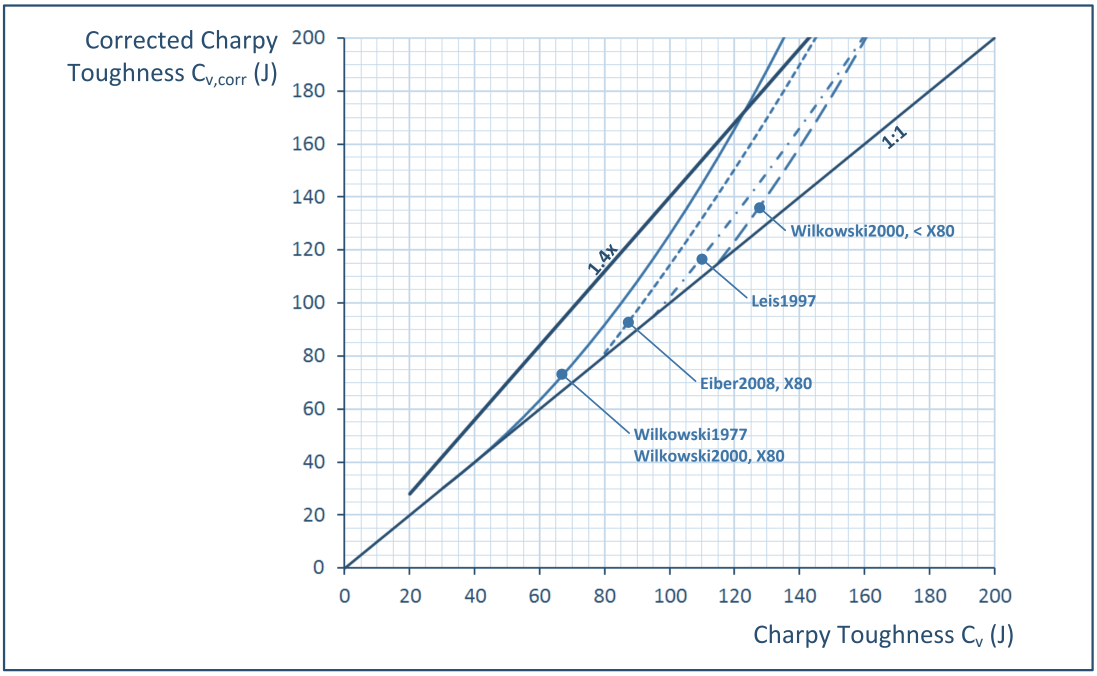


Figure 3-7: Comparison of Charpy arrest toughness correction models.

3.4.4 Decompression wave speed

Battelle calculated the decompression wave speed using an iterative simulation of the decompression process, in which the acoustic velocity and bulk fluid velocity are both calculated.

The acoustic velocity is determined first from fluid compressibility along the line of constant entropy (isentrope)[6]:

$$V_a = \sqrt{\left.\frac{dP}{d\rho}\right|_s} \tag{3-21}$$

As the decompression wave propagates down the pipe away from the crack, the fluid behind the decompression front accelerates towards the leak site. The actual velocity of the decompression front is calculated by subtracting the bulk velocity of the fluid from the acoustic velocity:

$$V_p(P) = V_a - U \tag{3-22}$$

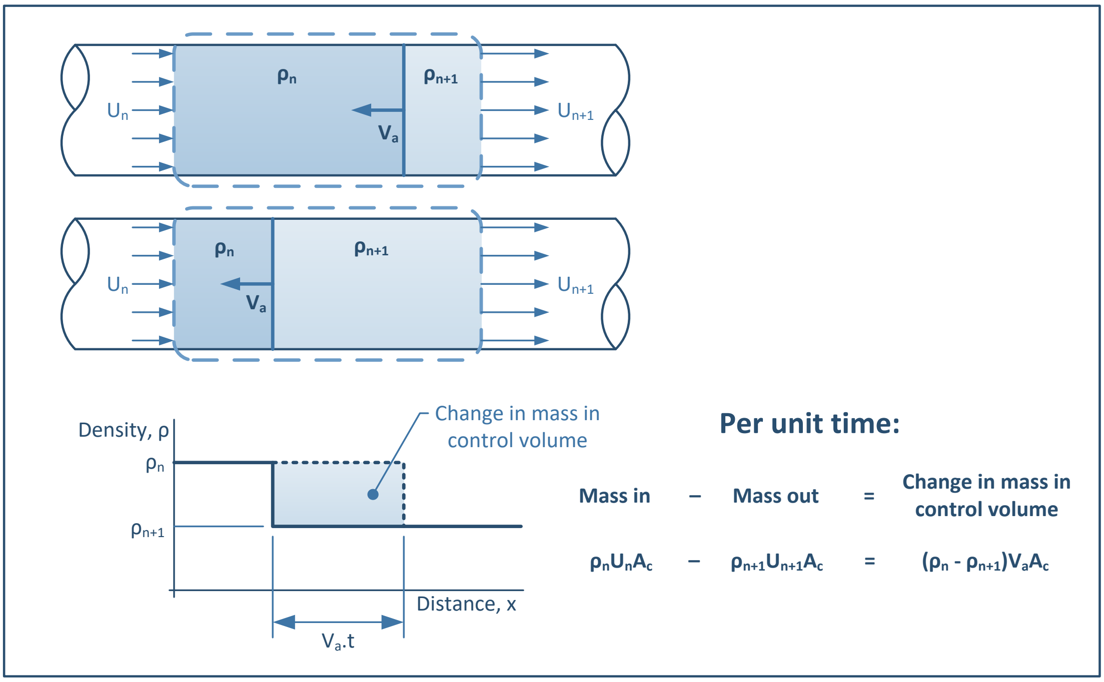


Figure 3-8: Velocity increase behind a decompression wave results from fluid expansion.

Expansion behind the pressure wave causes the increase in net fluid velocity towards the crack (where it flows out into the atmosphere). In the BTC method, the pressure wave is simulated in discrete pressure increments, and the change in velocity resulting from each increment is due to the

[6] The speed of sound is normally undefined within the two-phase region. However, it is customary to approximate it by considering the properties of the mixture under the homogeneous equilibrium mixture assumption (HEM)

incremental gas expansion, as per the following equations, illustrated in Figure 3-8.

$$U = \sum_{0}^{s} (\Delta U)_s \tag{3-23}$$

$$(\Delta U)_s = (V_a)_s \frac{(\Delta \rho)_s}{\rho_s} \tag{3-24}$$

3.4.5 Equations of state

Simulation of the decompression wave requires that the density and acoustic velocity be expressed as a function of pressure, and this relation is developed by assuming an isentropic process.[7]

A key input to this is the equations of state (EOS) of the fluid. Some contents, such as pure gases that remain in the vapour phase throughout decompression, may be approximated with simplified equations of state, such as the ideal gas equation. Mixtures generally exhibit more complicated behaviour.

Some fluids will exhibit a phase-change during decompression. At the phase boundary (the saturation pressure) the density of the fluid decreases at constant pressure. The effect of this is to cause a horizontal plateau in the decompression curve. See Figure 3-9 for illustration of isentropic decompression paths for a typical gas mixture, including phase boundary behaviour.

Research conducted by the Energy Pipelines Co-operative Research Centre (EPCRC) compared several different Equation of State models [21]. They found that equations published in 2008 by the Groupe European de Recherches Gazières, GERG–2008, outperformed other models for pure CO_2 and CO_2 mixtures [22]. For this reason, it is recommended that the GERG equations of state be used in ductile fracture arrest calculations for gas mixtures.

The GERG equations have been incorporated into EPCRC's two-curve model software, EPDECOM [23], which is discussed below.

[7] An isentropic process is adiabatic (no external heat input) and reversible. This assumption implies that there is no effect of friction against the pipe wall, and that the mixture is homogenous.

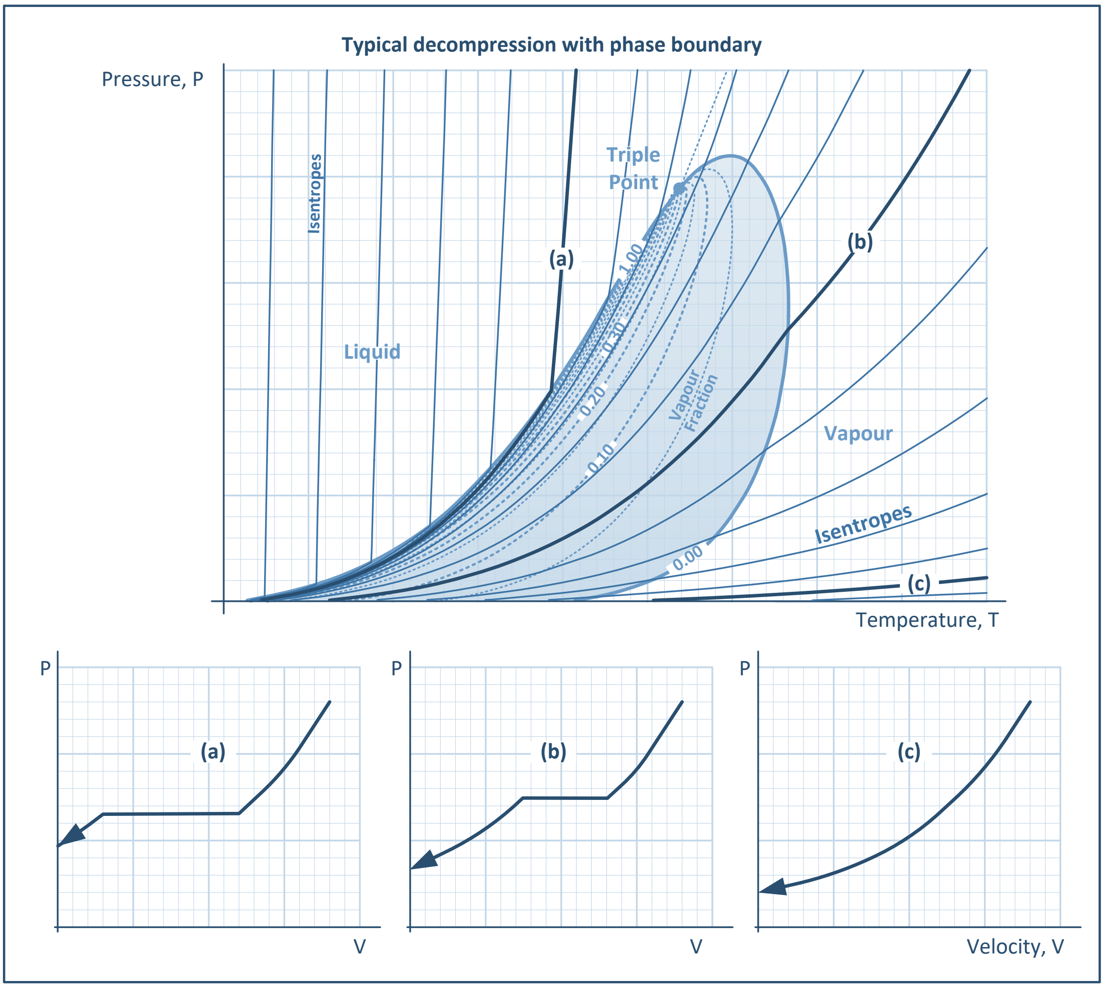


Figure 3-9: Decompression curve response to crossing the phase boundary.

3.4.6 Battelle short-form equation

In a specific range of conditions, the Battelle short-form equation can be used. The formula is provided in Equation (3-25) (in SI units) [15].

$$C_v = (3.57 \times 10^{-15})\sigma_h^2 (Dt)^{1/3} \tag{3-25}$$

Due to the approximations made in developing the equation, it may only be applied to pipelines with internal pressure less than or equal to 15.3 MPag, transporting lean gas, and not made from X80 or higher grade steels. The results are also considered inaccurate if the calculated full-size Charpy toughness is greater than 95 J—the results could still be acceptable if a correction factor should be applied, but under AS 2885.1 the longer-form BTCM is required above this point. Note this equation is not normally provided in S.I. units.

Lean gas is defined in AS/NZS 2885.1 as “almost entirely methane”, and having not more than 5% ethane, and not more than 1% heavier hydrocarbons.

3.4.7 Fracture propagation in carbon dioxide service

Pressurised carbon dioxide forms a 'dense phase' fluid. When it decompresses, it undergoes a phase-transition at near-constant pressure, this causes a long horizontal leg in the decompression velocity curve (similar to graph (a) in Figure 3-9). The initial drop in pressure is rapid, which is advantageous, but instead of further decreasing, the pressure at the crack tip remains at or just below the saturation pressure (the pressure of the phase transition) over a wide velocity range. In contrast to rich natural gas mixtures, the saturation pressure is observed at a high fraction of the initial pressure. This results in a high arrest-toughness requirement for CO_2 pipelines. The Battelle Two Curve Method, as described above, has not been successful at predicting the arrest toughness.

An empirical model was developed for use with CO2 pipelines [24], which has been incorporated in standard DNV-RF-F104. The model is based on arrest and propagate pipes from nine full-scale propagation tests. The fracture conditions and pipe properties were characterised by two dimensionless variables, the non-dimensional fracture resistance R_f and a crack-tip stress ratio, R_σ, defined (in SI units) as follows:

$$R_f = \frac{\pi G_D E}{24\sigma_f^2\sqrt{Rt}} \tag{3-26}$$

$$R_\sigma = \frac{\sigma_h}{\sigma_f} \tag{3-27}$$

Here σ_h is the hoop stress due to pressure at the crack tip, which would usually be estimated by the Barlow formula (Equation (3-2)) at the saturation pressure, which is determined from published equations of state or shock-tube testing. It has been observed that the predicted saturation pressure is higher than the actual pressure at the crack tip, due to a pressure drop across the saturation plateau, the reason for which is not well understood. Using the predicted saturation pressure is consequently conservative, since the empirical model was calibrated using the measured crack tip pressures from the full-scale tests.

The empirical model provides an envelope for fracture arrest, shown in Figure 3-10, and defined by the following three boundaries:

$$\begin{gathered} R_f \geq 3.1 \\ R_\sigma \leq 0.27 \\ R_\sigma \leq 0.021R_f + 0.1649 \end{gathered} \tag{3-28}$$

This work should be applied with caution because validation of this empirical CO_2 ductile fracture model has only been conducted on a discrete range of X65 steels, at a discrete range of testing conditions, as detailed further in the following section.

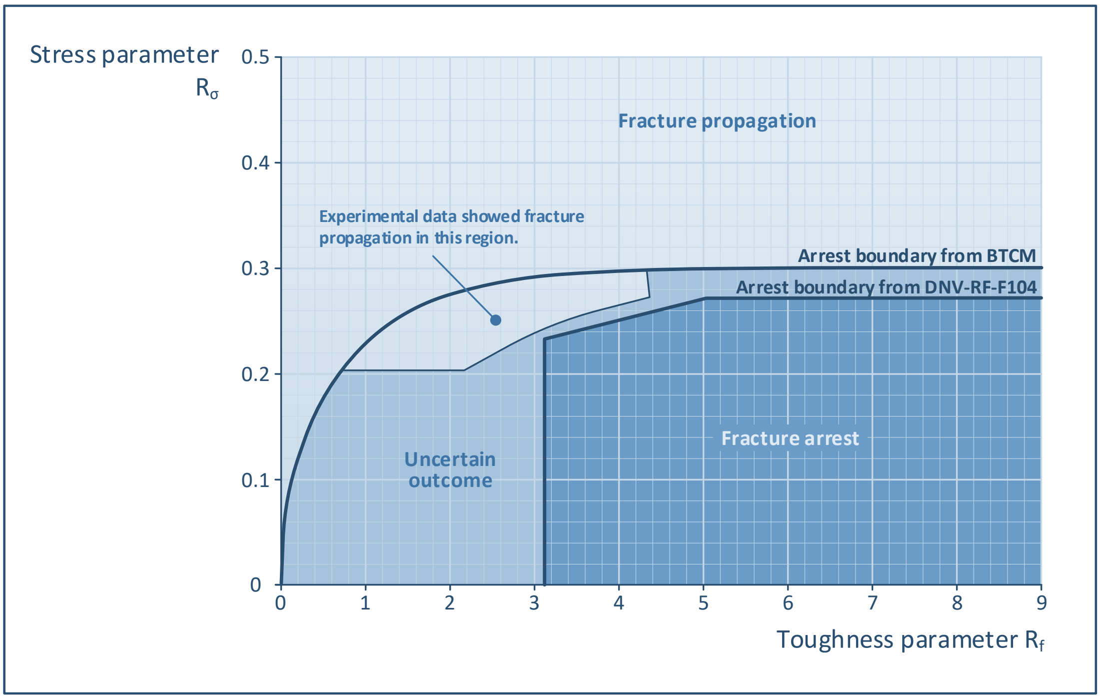


Figure 3-10: Carbon dioxide pipeline fracture arrest boundary.

3.5 Experimental validation

The above pipe fracture models have been subject to experimental validation as they have been developed over the past several decades.

Several test methods are used to simulate full-scale fracture—

- *The West-Jefferson burst test* involves a single pipe length with end caps and can be used to predict arrest behaviour and fracture appearance in a single pipe.
- *Full-scale burst tests* involve several pipe lengths and are used to validate propagation and arrest behaviour. Reservoirs are installed at either end of the pipe string so that reflection of the decompression wave will not impact arrest behaviour.

Full-scale burst tests are expensive, and full-scale burst tests have been completed worldwide cover only a limited range of conditions (strength, size, toughness etc.).

Historical burst tests used to calibrate the original BTCM were conducted in the 1960s and 70s. Over time, further investigations focussed on difficulties with fracture propagation prediction at high toughness and high strength; the ranges of test conditions and mechanical properties of high-strength pipe (Grade X70 and higher) used in full scale burst tests between 1980 and 2015 are shown in Table 3-2 [25].

Table 3-2 : Full-scale burst tests

Variable	Range for material grade:		
Grade	X70 / 75	X80	X100
Number of pipes	34	30	31
Pipe nominal diameter	36 – 48”	24 – 48”	36 – 56”
Pipe wall thickness, mm	18.3 – 25.4	14 – 18.4	13 – 20
Yield stress (σ_Y^*), MPa	482 – 698	537 – 683	663 – 876
Ultimate tensile stress (σ_u^*), MPa	589 – 761	621 – 818	762 – 919
Yield/tensile ratio ($R_{Y\text{-}T}$)	0.77 – 0.94	0.79 – 0.92	0.84 – 0.98
Charpy energy (C_v), J	81 – 275	64 – 322	126 – 355
Initial gas pressure, MPag	11.6 – 18.2	11.2 – 18.5	12.6 – 22.6
Test temperature, °C	3 – 12	5 – 19.4	8.5 – 20
Backfill depth, m	0.9 – ~1.3	0.5 – 1.5	1 – 1.15
Gas composition	Air, Natural gas	Natural gas	Air, Natural gas
Methane level	100%	87 – 100%	96.5 – 98%

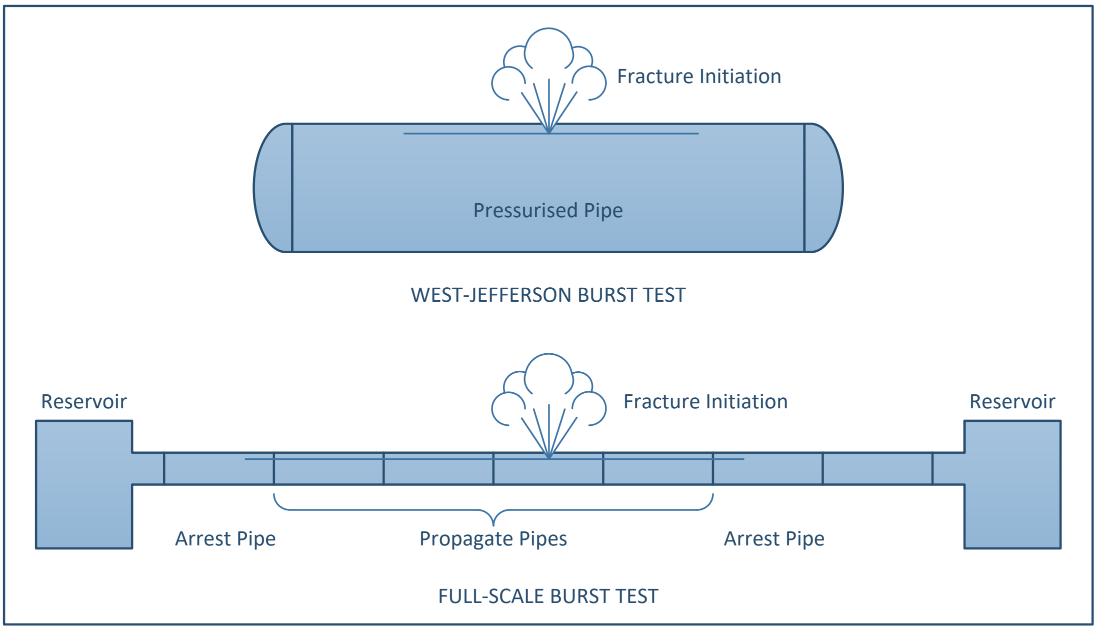


Figure 3-11: West Jefferson and full-scale burst test set-ups.

Data from full-scale propagation tests conducted on CO2 pipelines is scarce. For the development of the empirical model, data from testing of about 50 pipes was used, 17 of which arrested the fracture and 33 propagated it. All tests were conducted on X65 pipe grades, on diameters from DN400 up to DN900, wall thicknesses from 6.1 up to 25.4 mm, and initial pressures from 8.85 to 15.16 MPag. Most pipes measured a full-size equivalent Charpy V-notch toughness in excess of 250J.

- *Shock tube tests* are used to determine the gas decompression curve, by propagating a decompression wave along a "shock tube".

The shock tube test setup is illustrated in Figure 3-12. These tests usually consist of a number of small diameter, smooth tubes. For example, the shock tube of NOVA Chemicals, Alberta, Canada, has an inner diameter of 38.1 mm and a total length of 42 m.

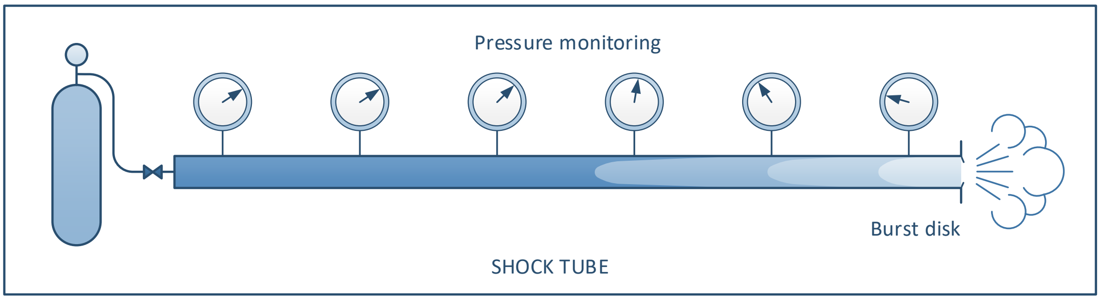


Figure 3-12: Shock-tube decompression speed measurement test set-up.

Prior to the test, a compressor is used to pressurise a variety of mixtures with defined composition. The test rig can ideally adjust the initial pressure and initial temperature such that different nearly isentropic decompression paths can be observed.

A rupture disc, precision-made to burst at a precise pressure, is installed at the front end of the tube. Sudden gas decompression is triggered when the disc ruptures, and the decompression wave then propagates towards the other end of the tube. A number of dynamic pressure transducers are mounted along the length of the shock tube, and these record the pressure trend at different locations during the test.

For any pressure level below the initial pressure, the time of arrival of the decompression wave at each successive pressure transducer can be determined from the measured pressure-time curves. The relationship between arrival time and distance from the rupture disc are almost linear because the effect of friction is negligible over the distances involved. The slope of the location-arrival time curve represents the decompression wave speed. Such calculations are repeated for progressively lower pressures, resulting in the gas decompression curve (pressure as a function of gas decompression speed) used in BTCM.

3.6 Pipeline fracture modelling software

Software tools exist to model both fracture initiation and fracture propagation.

With due care and expertise, a general Finite Element Analysis (FEA) software package can be used as an assessment tool for fracture initiation. These cannot, however, support an assessment of a running ductile fracture problems in an industrial context. This remains, for the time being, a research exercise (see section 3.7.3 for further discussion).

Fracture control models providing analytical solutions can be implemented in code for ease of use and parametric studies.

3.6.1 Fracture initiation software

Kiefner & associates provide calculation spreadsheets (KAPA) for failure pressure assessments on pipe affected by either a blunt metal-loss defect or a crack-like defect. The spreadsheet implements several published methodologies.

The spreadsheet is freely available, along with supporting information in the form of FAQs, from Kiefner & associates' website, kiefner.com/news/publications/. For blunt metal-loss defects, such as those caused by corrosion or removal of damaged metal by grinding, KAPA calculates the estimated failure pressure according to three methods: ASME B31G, the "Modified B31G" method (also known as the "0.85-dL" method) and the "Effective Area" method.

For part-through-wall crack-like defects, such as those caused by stress corrosion cracking, KAPA calculates the estimated failure pressure according to a *modified* log-secant formula.

With due care and expertise, a general Finite Element Analysis (FEA) software package can also be used as an assessment tool for fracture initiation. There are support packages available for this also.

3.6.2 Fracture ductile propagation software

The computation of a decompression curve requires numerical computations, which are best provided using a dedicated software package.

PIPE-DFRAC is a software package provided by PRCI and has been used by the pipeline industry for many years. PIPE-DFRAC combines the isentropic decompression tool GASDECOM and the numerical routines required to calculate the minimum required toughness using the Battelle Two-Curve Method.

Designs involving the transport of rich gas, complex mixtures as well as CO_2 mixtures can use APGA's software package EPDECOM. For fracture velocity, EPDECOM provides a strict implementation of the NG18 equations used in the original BTCM. Modern equations of state, in particular GERG 2008, are used in the prediction of the decompression wave speed, providing better predictions of the toughness requirement. Several correction factors are available in the software as required.

3.7 Current and ongoing research

Around the world, new research is being undertaken to increase knowledge about fracture mechanics in pipelines. Prominent research topics include fracture initiation, fracture propagation, characterisation of legacy pipeline materials for which fracture experimentation data is not available, and improved understanding of hydrogen embrittlement.

One of the most significant areas of research is ductile fracture control, as it applies to modern materials with significant toughness demand. The existing BTCM can be applied for most pipelines, but the limitations of this methods are relevant and problematic for large diameter pipelines and pipelines with rich-gas or dense-phase contents.

Some of the latest and promising research in this area is summarised below.

3.7.1 EPCRC fracture velocity model

The Energy Pipelines Co-operative Research Centre developed an alternate fracture velocity model [26]. This model seeks to make improved assumptions relating to the 'characteristic strain' for which the plastic wave velocity (and hence the fracture velocity) is determined.

As noted in Footnote 4, the Battelle fracture velocity model assumes that the area below the stress-strain curve up to the characteristic strain is related to the Charpy energy.

The EPCRC model seeks to use the tensile properties of a material (yield strength, tensile strength, elongation to failure, etc.) to further understand the plastic wave ahead of a propagating ductile crack, and redefine the characteristic strain.

The true stress-strain curve is assumed to take the form of the Hollomon equation:

$$\sigma = A\varepsilon^{n} \tag{3-29}$$

The strain-hardening exponent, n, is a key variable in the new model.

The EPCRC model generates the following equation for fracture velocity, in lieu of Equation (3-18):

$$V_{pl} \propto \beta \frac{\sigma_Y}{\sqrt{G_D}} \tag{3-30}$$

Where:

$$\beta = \sqrt{\frac{n}{n+1}} \frac{1+e^n}{R_{YT}} \left(\frac{\varepsilon_t^*}{\varepsilon_u^*}\right)^m \tag{3-31}$$

The fracture velocity is also assumed to be related to the backfill depth of the pipeline, by:

$$V_{pl} \propto \left(\frac{H_0}{H}\right)^k \tag{3-32}$$

The new fracture velocity model modifies the Battelle model (Equation (3-19)) to the following:

$$V_f = C\beta \left(\frac{H_0}{H}\right)^k \frac{\sigma_Y}{\sqrt{G_D}} \left(\frac{P}{P_a} - 1\right)^{1/6} \tag{3-33}$$

The EPCRC model acknowledges that the problem of using the Charpy toughness and flow stress to predict the limiting plastic wave-speed in the material.

3.7.2 SZMF Model

Salzgitter Mannesmann Forschung GmbH (SZMF) developed an equation for calculation of a pressure dependent crack velocity [27].

The method is based on energy methods, similar to what is used for brittle fracture propagation. It was assumed that the running fracture is arrested when the characteristic material's dynamic toughness (G_D) is greater than the accumulated elastic distortion energy (G_{el}), which is calculated using Equation (3-12).

$$V_f = C \frac{D_m}{t^2} \frac{G_{el}}{G_D} \left(\frac{P}{P_{a,geom}} - 1\right)^m \tag{3-34}$$

In this method, the constants are C = 12 and m = 0.343.

The arrest pressure used in this equation, and the velocity calculated by the equation, are both adjusted relative to a "reference" geometry. The fracture velocity is adjusted as follows:

$$V_{f,geom} = V_f \left(\frac{D_{m,ref1}}{D_m} \frac{t}{t_{ref1}}\right)^2 \tag{3-35}$$

The reference geometry for this equation is $D_{m,ref.1} = 1422$ mm and $t_{ref1} = 17$ mm.

The equation for the arrest pressure is adjusted as follows:

$$P_{a,geom} = \begin{cases} (P_a)^{C_2} & \Leftarrow \quad C_2 \leq 1 \\ P_a & \Leftarrow \quad C_2 > 1 \end{cases} \tag{3-36}$$

Where

$$C_2 = \frac{D_m}{D_{m,ref2}} \sqrt{\frac{t_{ref2}}{t}}$$

The reference geometry for this adjustment are $D_{m,ref2} = 1052.5$ mm and $t_{ref2} = 14.3$ mm.

The material toughness used in this method is taken from correlation with drop-weight tear test (DWTT) results, in which the adsorbed energy (C_w) has been measured. The following equation determines the material's dynamic toughness:

$$G_D = C_1 (C_w)^s$$

Where C_1 and s are constants; $C_1 = 282.8$ and $s = 0.3386$.

3.7.3 Numerical models

Several attempts have been made to apply numerical modelling to simulate and assess running ductile fractures in pipelines, which are reported in academic literature. Some of these employ general FEA software and for some, more special-purpose programs or subroutines have been developed.

An argument that is advanced for the use of numerical modelling is the improved chance of capturing coupled fluid-structure effects (in semi-analytical models such as BTCM, the fluid and the pipe structure are treated as uncoupled) and the ability to incorporate refined models of failure.

There are numerous limitations in developing numerical models. Foremost, the computing effort and time is great due to the range of scales at play (failure process, pipe wall thickness, pipe diameter, pipe length, test length) and the level of relevant details (backfill, coupled fluid dynamics, etc). The number of elements through the pipe thickness may be important, depending on the failure mechanism used in the model, which increases the model size and computation time.

A second limitation is related to non-linear scaling, stress state dependence, and strain-rate dependence of the material's behaviour compared between laboratory-scale tests to full-scale propagation behaviour. These factors are rarely captured adequately by numerical models. Mesh dependency on the numerical results can also introduce further bias.

A third limitation is the number of parameters that models may require, compared to the information reported in previous full-scale tests. The number of full-scale tests that can be used to validate a model is often limited (if not nil) unless assumptions are made about unreported properties required by the model.

Another limitation is that these models rarely provide the answer to the actual question posed by the designer: "What are the minimum required material properties that guarantee the arrest of a running ductile fracture?". This is the question that the BTCM answers, and is of practical industrial value.

Numerical models rather provide an answer to the question: "Would a pipe, with a given set of properties, arrest a running fracture when placed at a given position in a full-scale test layout?" Although this answer also has value, it requires an estimate of the properties beforehand to obtain an answer, unless numerous simulations are carried out.

Simulating several cases is often not practical due to the computation time associated with a single simulation. The advance in computing performance is progressively alleviating this limitation, but simulation times of days or even weeks are still likely for detailed models.

The following published numerical models are particularly notable:

- The *Picpro* software developed by Centro Sviluppo Materiali (CSM) (see e.g. Fonzo *et al.* [28]) utilises a cohesive zone model to simulate crack propagation. The cohesive zone model is calibrated towards a critical crack-tip opening angle (CTOA). The program has been used to establish calibrated expression of the applied CTOA as a function of key parameters.
- Scheider *et al.* [29] have applied the general purpose code ABAQUS with a user-defined "cohesive zone element". The cohesive zone behaviour is calibrated from small-scale tests. By applying a scheme where the pressure field moves with the crack tip and applying different crack velocities, the FEA has been used to derive an alternative pressure vs crack velocity curve.
- *SINTEF* developed a coupled numerical fluid-structure framework where LS-DYNA shell element simulations are coupled with a 1-D model for the fluid behaviour [30] [31] [32]. The failure process is modelled using a Cockroft-Latham ductile failure criterion. The model has been extended to consider different backfill conditions using Smooth Particle Hydrodynamics (SPH). The calibration of the backfill model is a challenging task in itself. A one-dimensional compressible

flow model is used to describe the pressure field downstream of the fracture. Fluid decompression and pipe deformation/fracture are coupled.

- Misawa *et al.* [33] and Misawa *et al.* [34] developed a model coupling the pipe deformation and fracture with the gas decompression. The model is different from other numerical models in that it enforces the shape of the deforming pipe based on an empirical shape function. It is a dedicated code that is not based on an FEA package. This model is particularly fast compared to other numerical models. The (incompressible) backfill is accounted for through energy conservation considerations. The deformation of the pipe is based on a set of equations describing the circumferential and radial displacements of the pipe wall as function of a "shape parameter". By considering the leakage from the opening crack, mass conservation for one dimensional flow is used to relate the flow fields (pressure, velocity, etc.) to the crack opening width at any cross-section of the pipe downstream of the crack tip.

  The conservation of energy of the displaced soil allows for the integration of the effect of the backfill into this model. The crack resistance (K_R) is assumed to depend on the crack velocity. The conservation of energy of the system is used to solve the problem in time step increments, accounting for the work done by gas pressure, the strain energy of pipe wall, and the kinetic energy, including that of the backfill soil and the crack length.

Whereas numerical approaches investigated in the literature contain several interesting features, none have yet become general tools in engineering assessment of running ductile fracture in pipelines.

3.7.4 Alternative approach to running ductile failure

3.7.4.1 Plasticity effects

The flow stress is the main plasticity parameter employed for the ductile fracture velocity determination currently used for the BTCM, especially in the case of high-toughness materials. Charpy toughness also implicitly integrates the notion of material plasticity. Some proposed modifications have also included features like the hardening exponent, n, and Y/T ratio, to incorporate a more detailed understanding of the material's plastic behaviour.

It is known, however, that strain rate may affect necking behaviour [35] and that anisotropy may also affect the load-carrying capacity of defect ligaments [36]. Both of these have potential to affect the ductile failure process and thus the arrest pressure. No systematic studies of these factors appear to have been undertaken in relation to running ductile fracture.

3.7.4.2 Alternative fracture parameters

In the BTCM model, the Charpy energy is used as the parameter expressing the toughness of the material, with an assumption that this value is correlated with the appropriate fracture toughness of the material, which is in turn predictive of the arrest stress *and* the fracture velocity. The DWTT energy is used in a similar fashion in some alternative models that have been developed, though are not in use by industry.

An alternative fracture parameter is the so-called crack-tip opening angle (CTOA). The CTOA represents the angle between the crack flanks just behind the crack tip. It is assumed that a critical value of this parameter can be defined above which fracture propagation will be arrested.

The CTOA is a dynamic counterpart to the crack-tip opening *displacement* (CTOD) used to assess fracture initiation from stationary cracks. The CTOA, for practical reasons, needs to be measured in the surface of the material, so in the case of significant crack tunnelling in the thickness direction of the material this may pose some conceptual challenges. Where the crack propagates as a 45° shear fracture, the tunnelling effect may be less important and the surface measurements may be representative for the whole behaviour in the thickness direction [37].

Application of CTOA has been proposed for assessment of running ductile fracture in pipelines, as already mentioned in relation to the *Picpro* software. The applicability of general fracture parameters in relation to propagating cracks is a somewhat controversial issue; there is logic behind trying to determine a toughness measure that is more directly related to crack propagation, like the CTOA. The determination of CTOA using DWTT has been standardised in ASTM E3039. However, the parameter has not gained widespread use in assessment of running ductile fracture in pipeline so far.

3.7.4.3 Plastic collapse

Leis [38] recently called for an alternative approach to describe running ductile fracture. The basis of the postulate is that while the NG18 model was reasonable for old pipes in which little toughness was available, the governing failure mechanism of tough pipes is rather a propagating plastic collapse. The final failure that occurs in the wake of this collapse is a consequence, not the governing mechanism.

This change of perspective shifts the view of the failure process away from that of a crack-tip process zone traditionally considered in fracture mechanics. The through-wall collapse is considered controlled by the ability of the material to sustain a strain in the circumferential direction. A high deformation capacity in the through-thickness direction could be even more important than the failure strain in the circumferential direction for delaying through wall collapse and increasing the resistance to crack propagation. Through-thickness straining would be affected by splitting or laminations occurring in the pipe plane and, consequently, accounting for such effects would be important in this framework.

The stress state developed through the wall thickness and its influence on applicable failure modes need to be better understood according to the author. Following such an understanding, the type of small-scale testing applied must possibly be reconsidered and alternative methods developed.

This postulation has not yet been followed by the development of an applicable model for quantitative assessment of running ductile failure.

3.7.4.4 Stress state dependent ductile failure

Some recent failure research has represented the strain at failure as a function of the material's stress state. This work shows promise for improving ductile fracture modelling of modern materials; ductile fractures are facilitated by void growth and coalescence in the steel, and the occurrence of this is strongly dependent on the three-dimensional stress state in the material, which can be characterised in a variety of ways.

The magnitude and type of stress at a location is dependent on the coordinate system used to define it. Some variables can be calculated that will remain constant regardless of the orientation of the coordinate system, which are called 'invariants'. Detail on invariants, and three-dimensional understanding of stress state, is provided in Appendix E.

Some more sophisticated failure theories exist that represent the failure strain of a material as a function of two other stress invariants: "triaxiality" and "Lode parameter" (also designated using the lode angle parameter) [39], [40].

At high triaxiality, the failure is dominated by the internal necking of ligaments present between voids of significant size. At low triaxiality, shearing of these ligaments dominates the failure process and the voids have limited size. Bao *et al.* related the failure strain of a material to the stress triaxiality, both experimentally and numerically. The work highlighted that different regions of the failure-strain to triaxiality relation exhibited different ductile failure modes [41], as shown in Figure 3-13.

The work of Barsoum & Faleskog [42] demonstrates the influence of the *Lode parameter*. This experimental work, on moderate and high strength steels, highlighted that at intermediate triaxiality, there was a significant change in failure strain accompanying a shift in the load parameter, which was associated with a change in the mechanism of failure from internal necking to shearing between voids. Similar to the conclusions of Bao [41], triaxiality alone was not sufficient to describe the mechanism of failure at intermediate triaxiality. Further investigation revealed that the effect of the Lode parameter on the failure strain was more significant for the high strength, low hardening steel compared to the moderate, high hardening steel [43].

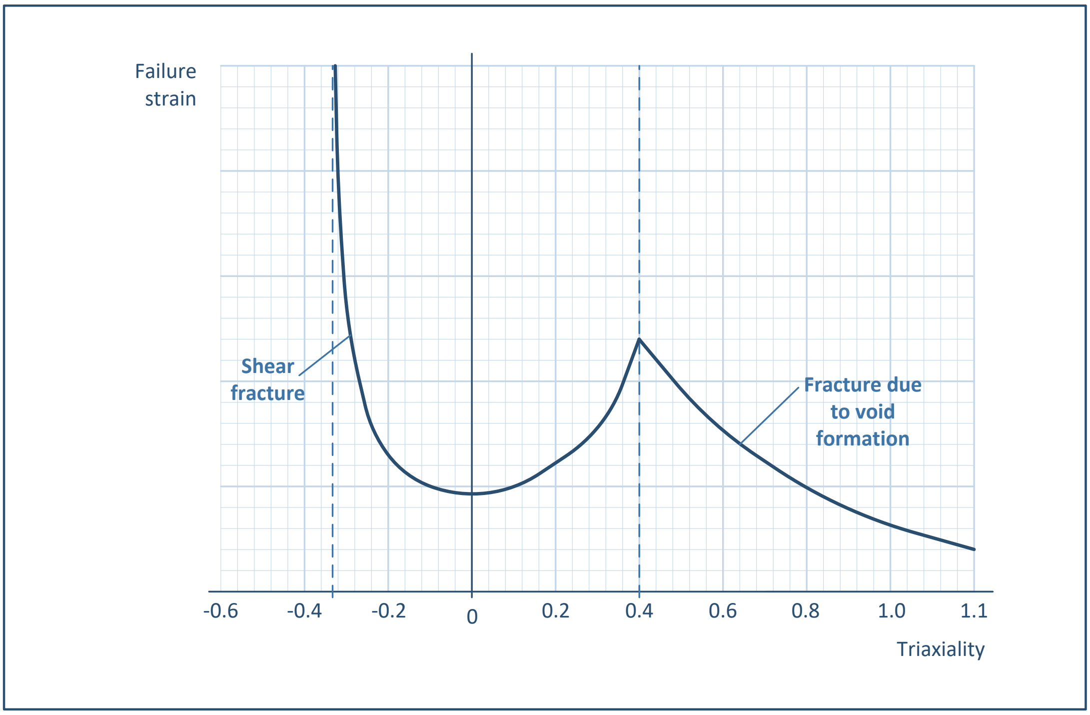


Figure 3-13: Example relationship between triaxiality and failure strain.

Bai & Wierzbicki [44] developed a plasticity framework by introducing both triaxiality and Lode parameter into the definition of the yield surface. Due to similarities between the yield surface and the failure strain locus, Bai postulated a failure strain locus function of these two stress invariants. An experimental method was developed to calibrate the empirical model. In parallel, Xue [45] proposed a damage plasticity model in the frame of continuum damage mechanics (CDM) with pressure and Lode angle dependency.

Later Bai & Wierzbicki provided a more theoretical grounding for the definition of the failure strain locus in the triaxiality / Lode parameter plane through a modification of the Mohr-Coulomb fracture criterion [46]. The Modified Mohr-Coulomb (MMC) transforms the original stress-based criterion into a strain-based criterion. The MMC predicts a hyperbolic dependence of ductility to triaxiality (fitting well existing work by Rice and Tracey [47]) The MMC also predicted asymmetric parabolic dependence to the Lode parameter. These relations are supported by experimental evidence.

Li & Wierzbicki [48] applied the MMC to the study of plain-strain fracture in the frame of continuum damage mechanics (CDM). He introduced a post-initiation softening function, which depends on the evolution of damage. This is ultimately a function of the stress state invariants, because it also depends on the failure strain locus and the equivalent plastic strain in the material.

These developments have been considered in other studies to investigate ductile failure in association with the failure strain locus. Although most of the developments have been linked to the automotive industry, an increasingly large number of studies have focused on line pipe steel behaviour.

Xue *et al.* [49] investigated fracture mode transition in ductile plates. Li *et al.* [50] applied the MMC to the study of mixed mode stable tearing. Lian *et al.* [51] applied the modified Bai-Wierzbicki model to the failure of high-strength steel sheets. Di Biagio *et al.* [52] studied the fracture propagation resistance of line pipe steel grade from X65 to X100. The study concluded on the importance of the strain hardening coefficient on the arrest performance of the pipe, in agreement with the MMC. Cao [53] applied Xue's model to the study of fracture propagation with remeshing. Novokshanov *et al.* [54] applied a modified Bai-Wierzbicki model to the study of ductile failure in X70 line pipe steel. Paredes *et al.* [55] studied crack initiation and propagation in X70 line pipe steels. Hojjati *et al.* [56] applied the modified Bai-Wierzbicki model to the study of ductile slant fracture of X70 pipeline steel under high strain rates. The strain rate dependant yield function was calibrated against split Hopkinson bar tensile tests followed by the simulation of Charpy tests.

Chapter 4 : Materials

The most common means of controlling fracture is by providing sufficient material toughness for the pipe to resist fracture over the applicable range of pressures and temperatures for the pipeline's safe operation.

In order to achieve this, two things are required:

1) That the relevant properties of the material (those related to fracture control requirements) can be appropriately *measured*, and
2) That a material can be *designed* to achieve the required properties.

Regarding item two (2) above, *designing* materials so that they have the right combination of properties required in service is very complicated. A material supplier is required to simultaneously meet strength, ductility, toughness, and transition temperature requirements (among others), such that they will not adversely affect weldability, and take into account changes in properties due to heat treatment during coating. Steel properties are determined from the steel's chemical composition and its thermo-mechanical processing or heat treatment history (particularly such factors as cooling-rate and reduction ratios and deformation, welding and heat treatment in the pipe mill). There are many different alloy designs and processing routes that steel and pipe mills can employ to achieve a set of mechanical properties.

Consequently, this document is concerned only with item one (1): understanding how the resulting mechanical properties of the pipe can be *measured*.

The mechanical properties of a pipe relevant to fracture mechanics are its tensile stress-strain properties and material toughness. This Chapter will provide a summary of the test methods and techniques used in the pipeline industry to measure tensile properties and fracture toughness, along with some guidance for material specification.

It should however be noted that the final performance of a modern steel will be dependent on other factors which are not, as yet, adequately quantified within the current set of historical mechanical property tests defined in Standards. These include inter alia, microstructural phases, banding, grain size distribution, segregation, inclusion type & content, distribution of microalloy precipitates and rolling texture, all of which can influence strain distribution, particularly in the process of fracture propagation. The occurrence of separations on the fracture surface of toughness specimens is but one unique feature which reflects variations in steel processing, which is discussed later in this chapter.

4.1 Tensile properties

The tensile stress-strain properties of a pipe are obtained by straining a material to failure and measuring the force and displacement during the process.

As described in Section 2.1, there are nine components of stress in a material (three normal stresses and six shear stress components). To test the material strength, it is standard to cause only *one* component of normal stress: uni-axial tensile stress, in a chosen direction. The results of such a test characterise both the elastic and the plastic collapse response for the material in that direction.

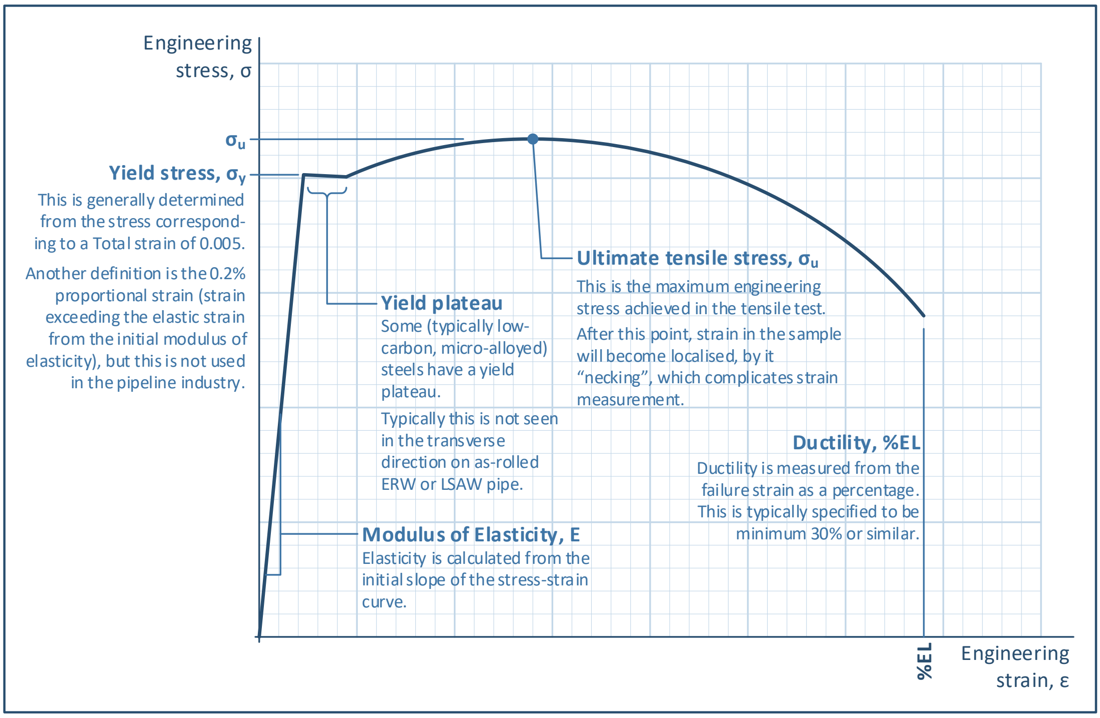


Figure 4-1: Engineering stress-strain results from a uni-axial tensile test.

The resulting stress-strain curve typically looks like Figure 4-1. From this curve, modulus of elasticity, yield strength, ultimate tensile strength, and ductility (measured from per-cent elongation at fracture, %EL) are normally reported and checked against specified limits. The Y/T ratio, R_{YT}, which is the yield stress divided by the ultimate tensile stress, may also be specified by the purchaser. Note that this figure shows a "yield plateau" phenomenon, though this is not typically seen in the transverse direction in welded line pipe.

The results of the stress-strain test are obtained as *engineering* stress and *engineering* strain. 'Engineering' stress and strain are calculated with reference to the original geometry of the stressed solid.

In reality, the solid deforms and becomes thinner as the strain increases, reducing the cross-sectional area. The true stress, being force *per unit area*, consequently increases above the engineering stress. This distinction is more significant in plastic, rather than elastic, conditions. In this document, engineering stress will be designated with an asterisk ($*$).

Engineering stress can be converted to *true* stress and strain for use in modelling:

$$\varepsilon = \ln(1 + \varepsilon^*) \tag{4-1}$$

$$\sigma = \sigma^*(1 + \varepsilon^*) \tag{4-2}$$

4.1.1 Uni-axial tensile tests

Uni-axial tensile tests are completed using a machine that grips each end of a specimen and stretches it until it breaks. The samples that are tested are wider at each end, with a narrower, parallel section in the middle (these are colloquially called "dog-bone" specimens because of their appearance), as shown in Figure 4-2. This shape ensures that the highest stress occurs in the narrower section and is mostly uniform across the cross-section.

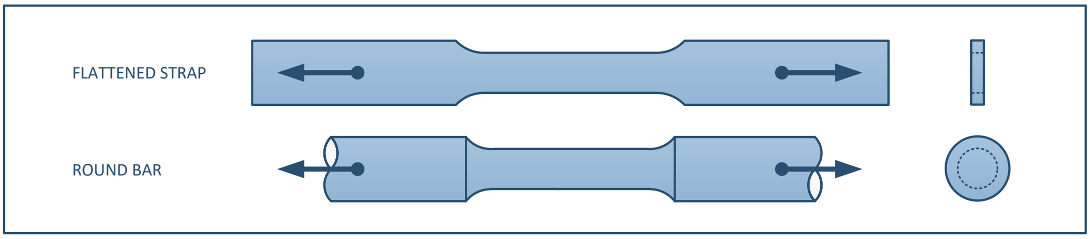


Figure 4-2: "Dog-bone" specimens for tensile testing.

Standardised requirements for tensile testing are specified in several international standards. The preferred standardised test regime used in the Australian pipeline industry is ASTM A370.

It is possible to test either a rectangular cross-section from the pipe (this is called a flattened-strap test) or a machined round bar, as shown in Figure 4-2.

Round bar specimens are polished and turned with the intent of discouraging the initiation of fracture from surface defects at high stress, which would underestimate the material ductility (cracks will more readily initiate at the corners on a rectangular specimen). Unfortunately, such specimens also suppress progressive yield behaviour that is actually observed in a pipe wall, introducing artefacts to the stress-stain curve such as upper and lower yield point and yield elongation. Yield strength is determined at 0.5% total strain which is insensitive to the effect of these artefacts.

Full-thickness rectangular specimens taken from pipe have to be flattened, which causes a small amount of work hardening and modifies the yield behaviour. Consequently, they are not the preferred method in AS 2885 for establishing the actual yield behaviour of line pipe.

It is not always possible to cleanly extract a round-bar specimen from a pipe wall, and machining this to a small diameter removes a lot of material making the test less representative of the through-wall properties. Selection of test type and orientation is made with reference to the pipe size and thickness. For pipelines with high design factor, ring expansion tests are used in conjunction with flattened strap tests, to gain confidence of the full-scale yield condition.

4.1.2 Ring expansion test

The ring expansion test is a testing method applicable for the testing of pipe tensile properties in the hoop direction in smaller-diameter pipe. This method is often mandated by Australian standard AS/NZS 2885.1, because the loading is more analogous to pipeline service conditions.

The standardised test method is ASTM A370 with AS 1855 providing valuable additional information. A ring of pipe floats unrestrained between two platens, and a rubber bag or seal is inflated inside the pipe ring to apply hoop stress to it. The pipe is loaded by internal pressure on the inner wall, which converts to hoop stress (by Equation (3-1)). Strain is inferred from change of the circumference, measured using an extensometer.

4.1.3 Test orientation

For large pipelines, the transverse (hoop direction) tensile properties are tested and reported. For pipes 6” diameter (DN150) and smaller, longitudinal testing is more common because samples can be extracted. The tested yield stress determines the pipe grade, even though the strength in the untested orientation may be different. Transverse data is preferred because the highest stress in the pipe is in the hoop direction due to pressure. Likewise the hoop properties are also relevant to the fracture mechanics of longitudinal defects.

In pipe stress analysis, the assumption is usually made that the pipe will be *isotropic*—that is, that the tensile properties in the longitudinal direction will be the same as the hoop direction. The design factors for longitudinal stress and combined stress (Von-Mises or Tresca) are expected to be sufficient to accommodate any anisotropy that may occur in reality.

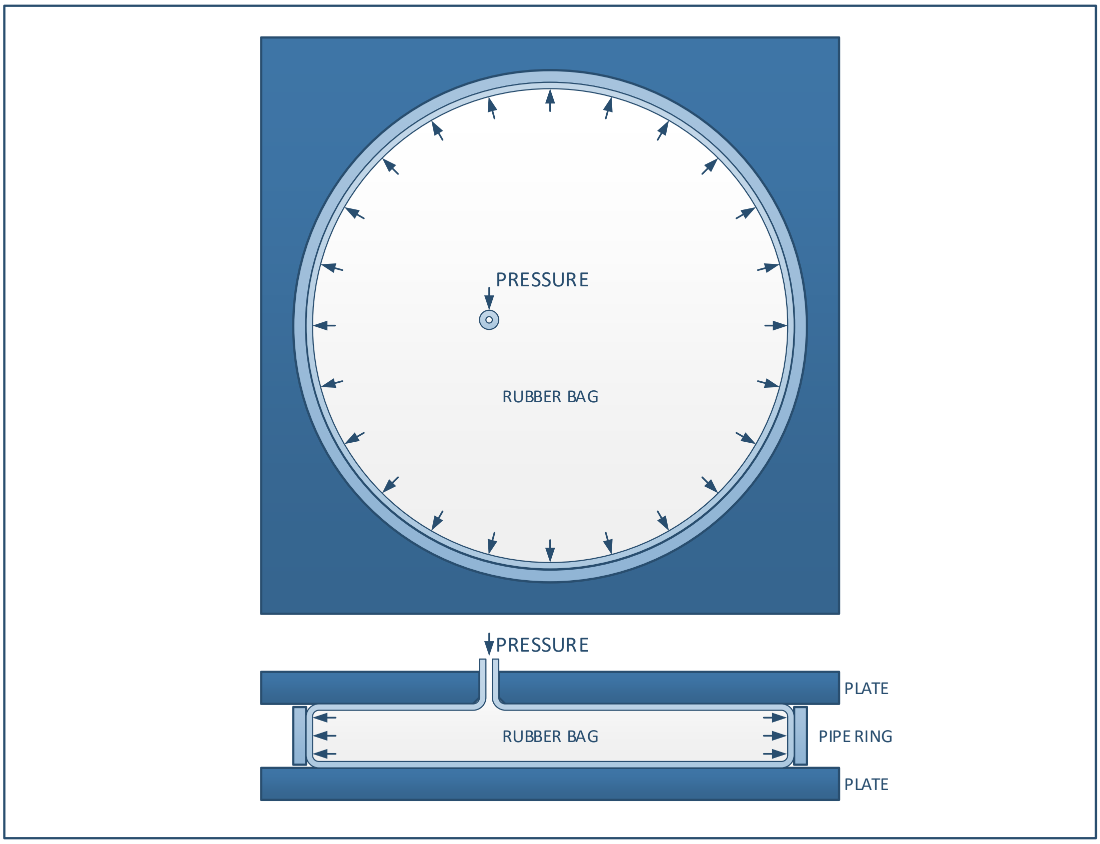


Figure 4-3: Typical ring-expansion test set-up with rubber bag for internal pressure loading.

Actual anisotropy in the material strength will occur due to the mechanical forming processes used to make the pipe. In the case of ERW pipe, the strip-rolling process will elongate inclusions in the steel to orient the along the rolling direction. The higher strength is typically measured perpendicular to the rolling direction, hence transverse strength would be greater than longitudinal. SAW pipe, however, is made from plate, which doesn't have as significant asymmetry in its rolling compared to strip, and the last stage of manufacture for this pipe is expansion in the hoop direction. In either case, the magnitude of strength anisotropy is usually between about 3 and 10%.

4.1.4 Relevance to fracture modelling

As described in Section 2.4, material plasticity has a significant role to play in material failure for ductile materials. The horizontal axis of the failure assessment diagram in Figure 2-8 describes when plastic collapse conditions predict the occurrence of failure. Plastic collapse conditions dominate failure more and more as the material toughness increases.

Consequently, both crack initiation and crack propagation calculations rely on the material's tensile properties to characterise the plastic zone around the crack tip.

It is important for fracture control analysts to recognise that real pipe properties will often exceed the specified minimum, potentially by a significant margin.

Line pipe yield and tensile strengths are specified as allowable ranges. For example, API 5L allows a range of 150 MPa above SMYS for X70 PSL2 line-pipe (though best practice supplementary specifications would limit this to 120 MPa or less). It follows that a flow-stress estimate based on the specified *minimum* will be an underestimate for many compliant steels.

Excess strength will typically benefit the pipe's tolerance to defects, especially in high-toughness pipe where the fracture is flow-stress controlled. However, experimental data shows that current fracture velocity models are insufficient at high strength steels, and so having high strength, though it improves fracture resistance, can make fracture prediction and modelling more difficult. In particular, excess strength can also correspond to a higher yield/tensile ratio, which should be reviewed. A significant practical issue caused by excess strength is difficulty achieving weld over-matching.

4.2 Toughness testing

A number of methods exist for measuring material toughness. The two significant production tests for the pipeline industry are the Charpy V-Notch (CVN) test and Drop-Weight Tear Test (DWTT). These tests are economical in the continuous manufacturing context.

There is also a large range of other tests that can be conducted that are more rigorous, and provide more direct quantitative results in terms of the fracture mechanics theory. These are often conducted in the context of fracture research and sometimes for weld qualification.

Details about these tests are provided in the following sections.

4.2.1 Charpy V-notch (CVN) toughness tests

The Charpy V-Notch impact test is a small-scale laboratory test that measures the toughness of a material. Charpy toughness has been used extensively in past research for empirical correlations to fracture behaviour; however, it is difficult to relate it clearly to the mathematically-derived toughness variables (G, K, J and δ) defined in Section 2.3.

In a Charpy test, a striker on a pendulum is used to impact a notched beam test piece that is supported by a pair of anvils. All elements of the test are standardised—the mass of the striker and its radius, the geometry of the pendulum, the geometry of the test piece, the geometry of the notch, and the requirements for the anvils.

ASTM E23 is preferred by the Australian pipeline standard over other Charpy test standards, such as ISO 148, and AS 1544.2, because its machine verification methodology yields more consistent results.

The standard size for the test piece is a cross section of 10mm x 10mm and a length of 55mm. A 2mm deep V-shaped notch is machined with precision to achieve the exact depth and notch tip radius. This results in a standard cross-sectional area at the notch, A_v, of 80mm^2 (in SI units, 8 x 10^{-5} m^2).

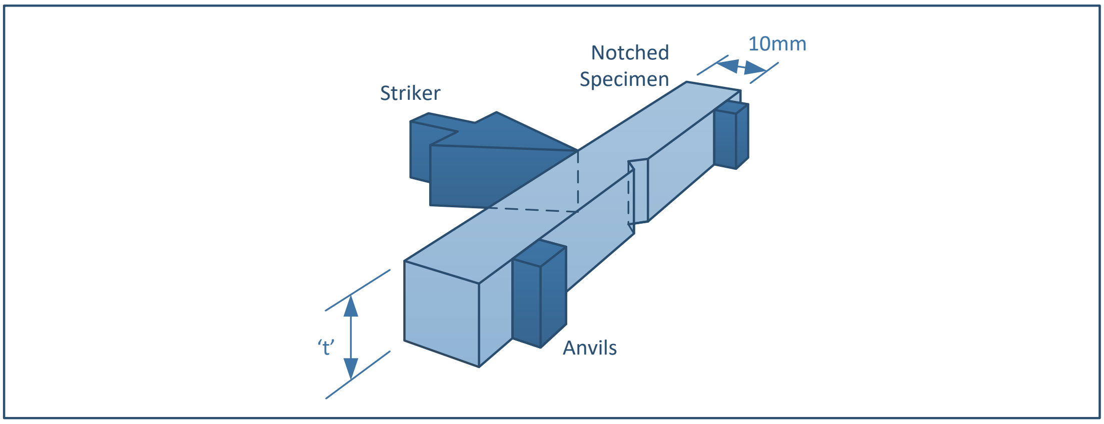


Figure 4-4: Schematic of Charpy testing set-up.

The test is completed by swinging the large, heavy striker through the sample, which is supported on each side. The striker hits the back of the sample opposite the notch. This often causes it to break into two pieces, though some samples with high toughness do not break apart, but rather bend enough to pass between the support anvils.

The results of a Charpy test include:

- *Charpy energy, C_v.* This is the energy absorbed in breaking the specimen, inferred from the difference in potential energy of the pendulum at the beginning and end of its swing.
- *Shear area, S_v,* also referred to as "fibrosity". This is the percentage of the fracture surface that has fractured in a ductile mode.

Due to the variability in individual results, the standard test protocol is to strike three specimens consecutively, and the stated Charpy toughness is the average of the three results. Nevertheless, minimum individual energy is often specified to limit the variability permitted for the material property. Also, under some standards, if the results have too much spread then more tests are conducted to increase confidence in the average.

The energy measured in a Charpy test includes both the energy to initiate the fracture and to propagate the fracture through the specimen. In high toughness steels it will also include the energy absorbed in plastic deformation at the location of striker impact and the anvils.

4.2.1.1 Interpretation of results

Charpy tests are intended to provide an approximation of the strain energy release rate, that is, the energy needed per unit fracture surface area to create a fracture. At low toughness values, where the effect of plasticity is low, they are generally effective and the correlation in Equation (4-3) may be applied:

$$G_C \approx \frac{C_v}{A_v} \tag{4-3}$$

That equation should also be applied when using the NG-18 equation (Section 3.2.1), because it was used to validate the method.

When Charpy toughness results are used to predict the critical stress intensity, K_{IC}, the above approximation will be insufficiently accurate for ductile materials. Several correlations have been proposed. Both API 579 and BS 7910 have correlations intended to convert between Charpy results and stress intensity factors, for use in analysis. The Rolfe-Novak correlation [57] is common for upper-shelf toughness correlation, provided here in SI units:

$$\left(\frac{K_{IC}}{\sigma_Y}\right)^2 = 51.7\frac{1}{\sigma_Y}\left(\frac{C_v}{A_v}\right) - 0.00635 \tag{4-4}$$

Though this correlation is not precise, it provides a basis for analysis when no other may be available. However, it is preferred to use a different method for measuring toughness if detailed fracture growth modelling is being undertaken.

Because the tests are dynamic, Charpy results are conservative when used as an approximation of *static* (initiation) transition temperature for a given thickness, an approximation that influences most pipeline standards. However, Charpy tests are non-conservative for estimating full-scale fracture propagation transition temperature. This can be corrected with reference to DWTT shear area results at the same temperature (DWTT is a closer to being a full-scale test, and is more representative) as per Equation (4-9) below.

4.2.1.2 Energy thickness adjustment

For small diameter and thin wall (< 10 mm) pipes, it is not possible to use standard Charpy impact specimens. For these cases, the standards for Charpy impact testing define acceptable subsidiary-size specimen thicknesses, 7.5, 6.7, 5.0, 3.3, and 2.5 mm thickness. Clause P.8 of Annex P in API 5L provides formula for determining the maximum specimen size that can be extracted from a pipe.

An alternative for subsidiary sized specimens, that maximises the wall-thickness tested, is to use a “gullwing” specimen from the full wall thickness. Where the notch is made, the curvature of the pipe is retained, whereas the ends of the specimen away from the notch are flattened as required to successfully test the specimen in a regular Charpy machine. The reason for this is that full-thickness provides the most representative results that can be achieved to establish Charpy toughness for the pipe wall, and hence provide the most accurate and least conservative result.

AS2885.1 allows two methods to convert the CVN result to a full-size equivalent CVN toughness: linear pro rating, or a power law relationship.

Linear pro rating is the default and more conservative option, returning a lower full-size impact energy. It assumes that the energy absorbed per unit area is constant regardless of specimen thickness:

$$C_{v,FS} = C_v \frac{A_{v,FS}}{A_v} \tag{4-5}$$

This method is suitable for low-toughness steels that exhibit flat fracture for most of the fracture surface and can be applied to older steels that have significant sulphur content (“dirty steels”). However, for fully fibrous fractures in high toughness steels, the energy absorbed per unit area decreases as the Charpy specimen size decreases.

One reason for variation in toughness with thickness is shear lip formation; as the specimen thickness decreases, the proportion of the fracture area taken by the shear lips increases. Another reason relates more broadly to stress triaxiality and material restraint. Ductile fracture growth initiates when there is a critical density of micro-voids; research has found that this critical density is reached at increasing levels of overall deformation for increasing Charpy specimen thickness [58].

The *power law* is suitable for these tougher steels, which takes the form of:

$$C_{v,FS} = C_v \left(\frac{A_{v,FS}}{A_v}\right)^x \tag{4-6}$$

The exponent, x, will vary, but is typically around 1.5. the actual exponent can be empirically determined if steel samples are available; otherwise AS/NZS 2885.1 advises that 1.5 is suitable to use for modern, clean steels (steels that have sulphur content less than 0.005%).

4.2.1.3 Transition temperature thickness adjustment

The thinner a Charpy specimen is, the lower its transition temperature. This means that sub-size Charpy results may have to be specified at a lower temperature. ASME BPVC VIII[8], for example, specifies a reduction in test temperature if the specimen is less than 80% of the width of the plate, for thicknesses up to 8mm. The maximum temperature reduction is 28°C, which applies to a 2.5mm specimen taken from a plate of 10mm or thicker.

When transition temperature is determined using Charpy test results, it is common to use the 85% transition temperature—that is, the temperature at which the shear area is 85% (and the toughness is would be about 85% of the way between lower-shelf and upper-bound values; see also Section 2.5.2). The 50% transition temperature is also sometimes used to characterise the transition temperature.

The following relation is taken from API 579 Annex F[9], and could be used to adjust 50% transition temperature from Charpy tests of different thickness.

$$T_{v,FS} = T_v - 51.4\ln\left\{2\left(\frac{A_v}{A_{v,FS}}\right)^{0.25} - 1\right\} \tag{4-7}$$

In the pipeline industry, however, it is common to use drop-weight tear tests and the 85% shear area threshold to define transition temperatures. The toughness results from Charpy tests have been found to vary linearly with shear area, S [5]:

$$\frac{C_v}{0.1 + 0.9S_v} = [C_v]_{S_v=100\%} \tag{4-8}$$

This equation enables an estimate of upper-shelf (S_v = 100%) and lower-shelf (S_v = 0%) toughness, from any measurements taken in the transition region. It also permits an estimate of full-scale propagation fracture resistance to be made by using the shear area results from a drop-weight tear test at the temperature of interest:

$$G_C \approx \frac{0.1 + 0.9S_w}{0.1 + 0.9S_v} \times \frac{C_v}{A_v} \tag{4-9}$$

These equations are based on a best-fit relation to actual data from 37 different CVN curves. They predict that lower-shelf Charpy energy will be 10% of upper-shelf Charpy energy.

4.2.1.4 Limitations of use

The aim of empirical toughness testing during manufacture is to provide fast and inexpensive laboratory-scale tests capable of predicting full-scale fracture behaviour. Historical research into pipeline fracture mechanics (presented in the previous Chapters) has generated models that correlate these laboratory-scale results with full-scale measurements taken from full-scale burst tests and

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[8] Refer Clause UG-84 of ASME BPVC VIII Division 1.

[9] This can also be found in BS 7910.

West-Jefferson tests. Outside the range where these models have been validated, it is not certain that the laboratory-scale tests have predictive value. This is especially the case for the Battelle Two-Curve method in high-strength or high-toughness steels.

4.2.2 Drop-weight tear testing

The Drop Weight Tear Test (DWTT) is used primarily to measure fracture appearance (i.e. the relative proportions of shear and brittle appearance on the fracture surface). It is an impact test similar to the Charpy test but uses a more massive hammer and higher impact energies to promote faster fracture speeds. The test specimen is also larger than a Charpy specimen. Tests are conducted on the actual material thickness (up to 19mm thick) with a fracture ligament 75mm long. Optionally, a sub-size specimen can be tested at a lower temperature. The standard test uses a pressed V notch.

Figure 4-5 below shows the test piece geometry and recommended partially flatted specimen profile from AS 1330. It is also acceptable to fully flatten the specimen.

There are three standards commonly used for DWTT: AS 1330, API RP 5L3, and ASTM E436. In the context of Australian pipelines AS 1330 is preferred because specimens which buckle or do not exhibit cleavage fracture from the notch tip are regarded as invalid by other standards (unless the complete fracture surface is ductile), whereas AS 1330 regards both these situations as valid. Note, at the time of writing (2018), AS 1330 is under revision to correct the test piece shape diagram and permit testing of smaller diameter pipe.

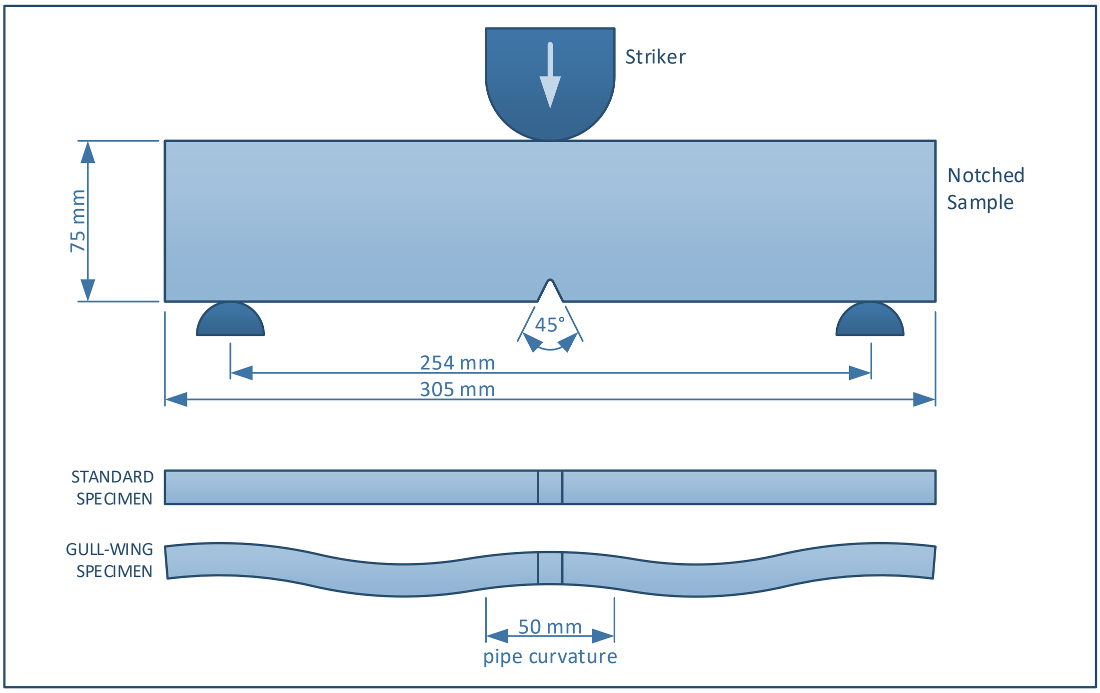


Figure 4-5: Schematic of drop-weight tear test set-up.

DWTT is used to measure shear area[10], S_w. The behaviour is representative of dynamic full-size fracture resistance. Research indicates that the transition temperature for 85% shear fracture appearance is a good estimation of the fracture propagation transition temperature. (Drop weight tear tests were developed when the effect of thickness on transition temperature was identified [59] [60]).

DWTT is conducted on two specimens, and the average shear area is required to exceed 85% to confirm that the behaviour is ductile at the test temperature. If the average is less than 85%, then additional tests may be conducted to increase confidence, as described in AS/NZS 2885.1 Appendix D.

4.2.2.1 Size limitations

Drop-weight tear testing is not feasible below 5mm thickness. Under AS/NZS 2885.1, it is not required below 5mm, because it is accepted that (due to low restraint) the transition temperature for such materials will be sufficiently low for any materials permitted under the standard (materials permitted under the standard must meet minimum toughness requirements).

With respect to diameter, AS 1330—2004 references testing only for DN300 and larger. API 5L requires testing on pipes that are DN500 and larger. In fact, it is possible to conduct the test on pipe sizes down to DN150; the EPCRC provides guidance on a method to determine the DWTT ductile to brittle transition temperature on such pipe (EPCRC Report RP6.1-04) using gull-wing or flattened specimens. Gull-wing specimens are preferred because the distortion that results from flattening can change the material properties in such a way that decreases the toughness (this is conservative, but not favourable to the pipe manufacturer who is required to meet specification requirements).

Down to DN150, it is also accepted to do testing on the strip or plate from which the pipe is manufactured. In theory, the results will be non-conservative, because the distortion of the plate during pipe forming should cause it's toughness to decrease. However, anecdotal evidence from industry indicates that this change has not been discernible. Consequently, AS/NZS 2885.1 is accepting of the lack of conservatism, noting that "with modern steels, the effect of pipe forming on fracture properties is usually very small".

4.2.2.2 Abnormal fracture appearance

In recent years, several phenomena have been observed in broken drop-weight and Charpy test specimens for heavy wall, high toughness steels that have been described as "abnormal" fracture appearance. Much research has been

[10] Drop-weight tear tests can also be instrumented to report fracture energy.

done to determine what the implication of these is for interpreting the test results.

The first is “inverse fracture”, in which the fracture initiates in a ductile mode but transforms to a brittle mode part-way through the specimen. This occurs in materials with high Charpy toughness and often the DWTT exhibits a high degree of plastic deformation during the test (this significant plastic deformation may be causing the change in fracture mode). Experts generally agree that inverse fracture is not a detrimental problem for fracture control; even where the fracture has failed in a cleavage mode, there is significant plastic deformation prior to fracture, so the fracture velocity will still be limited by the plastic wave speed.

The issue is, however, particularly relevant to the validity of the test under the testing standard. There is some uncertainty in interpreting the results of DWTTs that do not meet the requirement of brittle initiation that are stipulated in API RP 5L3. While the occurrence of inverse fracture is acknowledged in that standard, the tests are considered invalid and there is no guidance regarding how to assess the results. Although it is not intended for onshore pipelines, DNV have addressed inverse fracture recently in their offshore specification DNV-OS-F101, now called DNVGL-ST-F101. This code allows ductile initiation if the specimen exhibits ductile fracture “on the complete fracture surface” (section B.2.7.2) and acknowledges that this is contrary to API 5L3.

EPRG has conducted West Jefferson tests alongside DWTT on pipes made from TMCP plate. The test results showed that ductile initiation, and hence inverse fracture, could not be reliably suppressed, regardless of the notch type. When the material did exhibit both inverse and regular behaviour at a certain temperature, the difference in terms of shear area was marginal. Results also indicated that DWTTs with inverse fracture were suitable to predict the pipe’s transition temperature [61]. Some guidance on interpretation of results has been incorporated into API recommended practice 5L3.

The second fracture appearance issue is “separation”, in which the crack surface can have slits along the rolling plane, parallel to the direction that the fracture propagated. The reason for the separation behaviour is the subject of much conjecture and ongoing research. It has been observed in both DWTT and Charpy test results, and also in full-scale burst tests. Causes are not fully understood but the phenomenon is often apparent in “heavily rolled” plate or strip and is sometimes attributed to texture effects. The effect does not invalidate the measured test result because structures that separate during testing are expected to exhibit the phenomenon in a real fracture situation also. Separations can noticeably affect the measured Charpy energy and they can lead to significant scatter between specimens that break and those than do not [62] [63]. However their effect on the arrest capacity of a pipe in full-scale conditions is less clear and no quantitative measure of this effect has been established [64].

4.2.3 Other toughness test methods

Fracture toughness tests all aim to measure the ability of a material to resist crack growth. A large range of standardised tests exist to measure the static material toughness with precision in terms of theoretical fracture variables – the stress intensity factor, K, crack-tip-opening-displacement, δ, or J-integral, J.

These are published by (among others) the American Society for Testing and Materials (ASTM), the British Standards Institution (BSI), the International Institute of Standards (ISO), and the Japan Society of Mechanical Engineers (JSME). The testing is very similar across all these organisations.

The tests usually involve straining a notched specimen using a universal testing machine (as are used for round-bar testing in Section 4.1.1) to test in tension or under three-point bending, as applicable. The strain is applied so that the crack growth is more likely to be stable for most of the test. The instrumentation required to measure load and displacement is common across virtually all test standards; the extension of the specimen and the force are both measured, and in some contexts other variables are measured such as the crack length.

These specimens are carefully prepared to a defined geometry, with the aim of eliminating unwanted effects; this includes fatigue pre-cracking[11] to create a sharp crack tip, side grooves which can eliminate shear lips, or additional restraints to prevent out-of-plane buckling.

The results of the tests may consist of a single-variable (K_{IC}, δ_C or J_C) or a crack growth resistance curve, which is a plot of the apparent resistance variable against the crack length. For cleavage fractures, the resistance typically drops off after the crack begins to grow. For ductile fractures growing by micro-void coalescence, the resistance usually increases as the plastic region becomes larger and the remaining ligament in the sample being tested becomes narrower.

[11] Cyclic loading is used to grow a crack from an initial machined notch. Because fatigue can occur at low stress cycles, the crack tip is very sharp with negligible initial plastic zone. Specified fatigue load varies between different standards.

4.2.3.1 Specimen types

There are number of specimens that are used to characterize fracture initiation and crack growth. The specific design of a specimen type may vary between standards. The configurations that are currently standardized include the:

- Compact tension specimen (CT),
- Single-edge-notched bend (SENB),
- Single-edge-notched tensile (SENT),
- Curved Wide Plate (CWP) for pipelines,
- Arc-shaped specimen,
- Disk specimen, and
- Middle tension (MT) panel.

These specimen types are shown in Figure 4-6. Standardised methods are also available for further machining the specimens to mount a clip-gauge to measure crack mouth opening displacement (CMOD).

The orientation of the specimen must be selected to align the crack in the direction that the toughness is intended to be measured.

The toughness of a material depends on the material constraint. Toughness calculated using the standardised test specimens will consequently vary from the full-scale behaviour of a pressurised pipe. Figure 4-7 shows the variation from the full-scale behaviour for several standard specimens.

4.2.3.2 Applications for the pipeline industry

Fracture toughness testing using these methods is most commonly used in the pipeline industry for testing girth weld defects.

A curved wide plate (pipe segment) specimen should provide a good testing solution, accurately capturing the effects of tension, thickness and pipe curvature. Such a specimen is very expensive and presents many difficulties to the fatigue pre-cracking process. Nevertheless, a pipe segment specimen can be used to validate results from a defect assessment, without any prior fatigue pre-cracking. In this case, accurate material toughness information would be previously measured by proper testing on fatigue pre-cracked small-scale specimens and correlated to the larger-scale results.

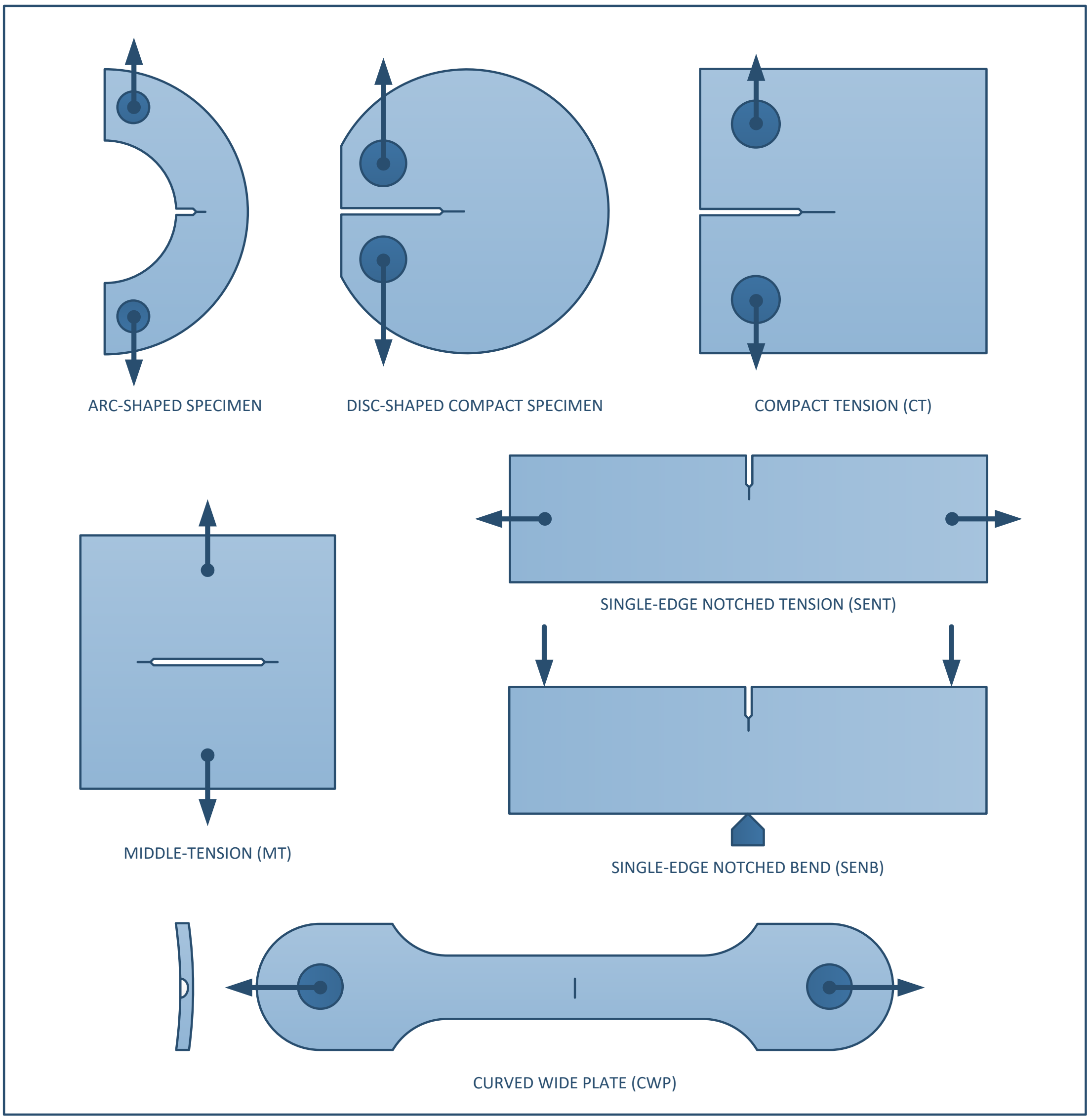


Figure 4-6: Standard specimens for fracture toughness testing.

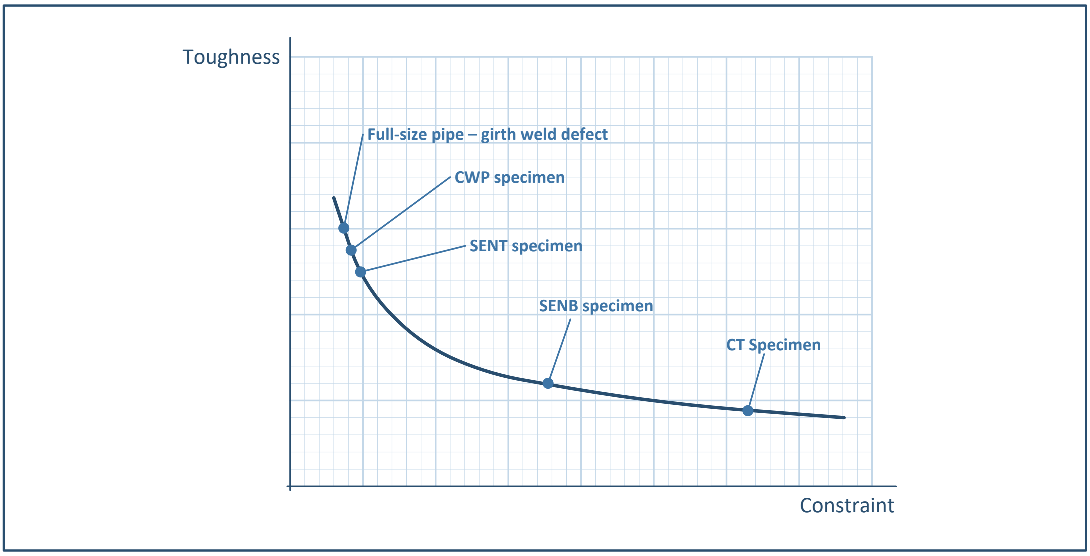


Figure 4-7: Restraint level comparison between various specimen types and full-scale behaviour.

Onshore pipeline Codes and Standards normally require Charpy testing for girth welds when workmanship acceptance criteria are used. For example, both AS/NZS 2885.2 and API 1104 require girth weld Charpy testing to demonstrate that fracture will be plastic collapse controlled rather than toughness controlled. This is deemed to occur where an average absorbed energy of minimum 40J (and minimum individual-specimen 30J) is achieved, based on the EPRG guidelines for the assessment of defects in transmission pipeline girth welds.

Currently, AS/NZS 2885.2 mandates CTOD testing when the acceptance criteria for girth weld discontinuities are per Tier 2 and the thickness is greater than 13mm. For Tier 3 acceptance criteria, CTOD testing is mandatory regardless of the thickness. API 1104 has a similar requirement and CTOD testing is mandatory when Annex A (alternative acceptance standards for girth welds) acceptance criteria are used. Both AS/NZS 2885.2 and API 1104 specify the use of SENB specimens for testing of the girth weld.

Although most onshore pipeline standards (such as AS(/NZS) 2885, ASME B31.4, ASME B31.8 and CSA Z662) require only the empirical Charpy or DWTT fracture toughness testing in most circumstances, offshore pipeline standards and in particular DNVGL-ST-F101 require crack-tip-opening displacement testing on SENB specimens on the weld metal. Testing of SENB specimens should be carried out in compliance with ISO 12135 and ISO 15653.

DNVGL-ST-F101 states that commonly used testing standards describe methods for determining the fracture resistance from *deeply notched* SENB or CT specimens. These specimens, both predominantly loaded in bending, have high crack tip constraint and will hence give lower bound estimates for the fracture resistance that can be used for conservative fracture assessment. Either SENB or SENT can be used, but SENT is recommended, because SENB is likely to result in unnecessarily conservative fracture toughness estimates.

4.2.3.3 SENB vs. SENT testing on pipeline girth welds

Onshore pipeline welding standards such as AS/NZS 2885.2 and API 1104 specify SENB testing to ISO 15653 (supplementary standard to ISO 12135) on the girth weld, when CTOD testing is required. However, as shown above in Figure 4-7, SENB testing is likely to result in unnecessarily conservative fracture toughness. Hence, DNVGL-RP-F108 recommends SENT testing in accordance with BS 8571 for pipeline girth welds.

CTOD tests can be performed for defining a single value fracture toughness or resistance curve (CTOD-R). In order to perform ductile tearing analysis to estimate the tensile strain capacity of the girth welds, usually the CTOD resistance curve (CTOD-R) would be required. A CTOD-R can be derived using the single specimen test method (unloading compliance) or the multiple specimen method, see BS 5871 and DNVGL-RP-F108.

4.3 Steel manufacture and specification

A pipe purchaser needs to ensure that the steel pipe order has properties sufficient to achieve fracture control. This will most commonly involve specifying limits for the minimum toughness and strength and maximum transition temperature and possibly the maximum yield-tensile ratio. A pipe specification will detail the testing that is required, the results that must be achieved, and the test frequency. Further detail of specification of fracture properties is provided below.

4.3.1 Statistical spread of steel properties

Pipe production does not result in an entirely homogeneous product. Steel properties vary between heats, within a heat and even within a single pipe. Superior manufacturing process control reduces variation, so good quality mills will be able to reliably produce pipe properties in the specified range. Nevertheless, testing methodologies require some use of statistics to accommodate the spread of data.

The following definitions are important for understanding the spread of data in a pipe population:

- *Test unit* – Mechanical tests are completed once for each test unit, hence the frequency of testing in a production run defines the size of a test unit. Test units for most tests are no larger than a single heat, and are often smaller to track specified variables better.
- *Production run* – this is a single continuous manufacture of pipe from a single pipe mill, consisting of one pipe grade, thickness and diameter.

For mechanical tests, the following variables are considered, defining the distribution of results:

1) *Test result* – this is the result of the test(s) conducted on a single pipe sample. The test procedure may require that multiple specimens are taken from the pipe sample, and the test result is the average of these specimens (e.g. Charpy test results are averaged from three specimens). This result is considered representative of the rest of the test unit.
2) *Test result (any specimen)* – this is the minimum test result of the specimens tested from a single pipe sample.
3) *Test result (any test unit)* – this is the minimum test result from any pipe samples tested in a production run.
4) *Test result (all test unit average)* – this is the mean test test result from all the test units in a production run.

4.3.1.1 Small pipe orders

Annex G in API Spec 5L states that the minimum average Charpy V-notch impact energy can be specified either for each test (the Test result (any test unit)) or for the order item (the Test result (all test unit average)). Clause 5.3.6.5 in AS 2885.1 states that the technical specification for mainline pipe shall nominate minimum Charpy toughness including, at a minimum, the minimum Charpy toughness (any test unit), and the minimum Charpy toughness (all test unit average).

With Charpy specification, it is possible to specify the mean toughness of the production run—i.e. the *Charpy toughness (all test unit average)*. However, for small pipe orders, two effects pose a statistical problem for specification of the mean.

Firstly, a significant portion of the pipe order might be less than the mean toughness (e.g. in an order with only three test units, there is 50% likelihood that *two-thirds* of the test units will be less than the mean and not all test units will represent an exactly equal volume of pipe). Secondly, the uncertainty of individual Charpy tests has an increasingly significant effect, especially for single test-unit orders.

The simplest solution for this is that, for orders with less than six test units, the *minimum* Charpy test result – i.e. the *Charpy toughness (any test unit)* – be specified in lieu of the mean Charpy test result (this is effectively the current requirement of AS/NZS 2885.1). Or, for short pipelines it may be feasible to review the pipe string for consecutive propagate pipes, and deliberately control the pipe distribution rather than relying on probability.

It is also advisable to ensure that there is a margin of safety in the measured toughness for small orders. Refer example in Appendix C.6 for further consideration of this issue.

4.3.2 Line-pipe specification to API 5L

Australian pipelines are most commonly constructed from API 5L steel line-pipe, to Product Specification Level 2 (PSL2). A supplementary pipe specification is usually warranted, to limit the permissible pipe properties and manufacturing processes within a narrower (or occasionally broader) margin than is permitted by that code. In Australia, this is required to meet the requirements of AS(/NZS) 2885.

A previous project conducted by the EPCRC created supplementary specification guidelines for both high-frequency electric resistance welded (HFW / ERW) and sub-arc welded (SAW) pipe [65] [66]. Several of these supplementary requirements relate directly to fracture toughness properties. These are summarised below in Table 4-1.

Table 4-1 : Common supplementary requirements for API 5L PSL2 line-pipe, which relate to fracture control.

API 5L Clause	Supplementary requirement
8.3.2	Open-hearth steel-making is not permitted. The Basic Oxygen Furnace (BOF) achieves low carbon levels necessary to reliably achieve a tough fine-grained structure.
8.3.3	Fine-grained micro-structure is also achieved by having soluble aluminium levels Al > 0.015 wt%
9.2.2	Again, promoting fine-grained structure and hence enhanced toughness, Carbon may be limited to be no greater than 0.013 wt%
9.3.2	Yield-tensile strength ratio (Y/T) is limited for pipes DN200 and larger. For pipe that will be coated in a process that will heat-treat, and hence strain-harden, the steel, Y/T may be limited to 0.90 prior to coating.
9.8.2	Charpy toughness minimum energy and test temperature should be as specified by the purchaser, not necessarily the API 5L default values.
9.9	Under current AS/NZS 2885.1 requirements, DWTT will often be specified lower than 0°C (-20°C is common). However, this document recommends that this is not required. Refer Section 6.2.3 of this document.
10.2.1.2	Charpy and DWTT testing frequency should be the same as for tensile strength testing.
10.2.3.3	ASTM E23 is demonstrably better than ISO 148-1 as it provides less variation in results, primarily due to more rigorous machine verification requirements.
Table 22	API 5L Table 22 says how to extract Charpy test pieces from unflattened pipe, however, for small diameter thin wall pipe where a Charpy cannot be removed in accordance with API 5L Table 22, testing may still be required. For this, it is common to test using 1/3, or even ¼ size Charpy test pieces, to use full thickness gull-wing test pieces, or to test the strip in lieu of the pipe.

API 5L Clause	Supplementary requirement
10.2.3.4	DWTT is completed to AS 1330, rather than the ASTM or API 5L standards, because those standards declare that a buckled sample is an invalid test; whereas buckled samples are accepted under the Australian Standard for DWTT.
G6 to G11	AS/NZS 2885.1 and this document are used to determine toughness requirements in lieu of sections G6 to G11.
-	Charpy and DWTT are also used to develop transition curves, recommended to be completed for 10 heats per production run.
-	Where coating processes heat-treat the steel and consequently cause strain-ageing, post-coating testing on a statistically-representative sample can be used to establish the change in mechanical properties caused by strain ageing. This could also be completed for ten heats per production run.

4.4 Heat treatment during coating

For some pipe coating options, the manufactured steel pipe is heated during the coating application. Exposing steel to elevated temperatures during coating will change its material properties.

The extent of the change in a steel's properties caused by heating depends on the magnitude of the temperature it reaches. Strain-ageing will occur at relatively low temperatures (above about 80°C). At much higher temperatures, stress relief, recrystallization, grain-size growth, precipitation hardening, austenitising and eventually melting will occur. For the higher-temperature effects, the final properties of the steel are strongly controlled by the cooling rate after heating. However, these effects are not a concern provided the line-pipe is not permitted to be heated above 260 °C.

Temperatures achieved during coating—most notably for fusion-bonded epoxy (FBE), which is common for Australian use—are typically in the range to cause strain ageing. The effects will apply to the entire pipeline.

4.4.1 Strain ageing

Strain-ageing of as-manufactured pipe in the pipeline industry occurs in low-carbon steels that have been work-hardened.

The effect of strain-ageing is to mostly increase the yield strength, with a smaller effect on the tensile strength. As such, it results in an increase of the yield-tensile ratio of the pipe. While, in theory, it can decrease the ductility (%EL) and toughness, in practice this has not been observed to any significant degree. The increase in yield-tensile ratio can adversely affect the plastic-zone behaviour and hence diminish effective fracture control.

The effect of strain-ageing is often accommodated by post-coating testing on some or all of the pipe order. The intent of the testing is to obtain an estimate of the characteristic increment in both yield and tensile strength and to verify that toughness is not materially affected.

At this time, it is often not possible to require that a pipe manufacturer:

1) Warrant properties in the as-coated condition, and

2) Duplicate the full production run test regime on the coated pipe.

It is therefore the intent of the as-coated testing to provide a statistically valid estimate of the incremental change in properties typically resulting from the coating process. This estimated increment is then applied to the as-rolled test results, to provide a data set for the pipeline.

It is acceptable to test only some heats, sufficient to establish statistically what is the effect of strain-ageing on the pipe properties. While AS/NZS 2885.1 specifies a minimum of 6 matched test result sets, more results increase the confidence in the estimate. Effort is well spent to ensure that the sample is unbiased and otherwise unaffected by any intended or unintended variation in behaviour at the coating plant. More than 30 results are likely unwarranted

The *as-coated* pipe is *not* typically required to meet the API 5L material specification limits, but rather a set of limits determined by the designer.

Yield-tensile strength ratio (Y/T ratio) is identified as a specific hazard from strain-ageing that is relevant to fracture control. API 5L requires that the Y/T ratio be at most 0.93 for as-manufactured PSL2 pipe in sizes DN400 and larger and grades up to X80. The EPCRC fracture velocity model suggests that in high strength pipe, higher Y/T ratios correspond to an increased toughness demand for ductile fracture arrest (Refer Section 3.7.1). That is, the fracture velocity model becomes very non-conservative for high Y/T. On this basis, AS/NZS 2885.1 requires the limit of 0.93 be extended to smaller sizes.

The limit on Y/T ratio is really required in as-constructed pipe. To achieve this, AS/NZS 2885.1 advocates specifying a limit of 0.90 (through supplementary specification) for as-rolled pipe in the expectation that the Y/T ratio of coated pipe will then, on the whole, not exceed 0.93. Certainly, close attention should be paid to Y/T ratios above 0.95.[12]

4.4.2 Hot-bends

Hot bends are not classified as pipes, but rather as fittings or 'components' under AS/NZS 2885.1. For convenience, these fittings are manufactured from line-pipe.

The temperatures used in manufacturing hot bends are higher than coating and the steel properties are significantly affected. The mechanical properties require being determined through careful process design (time at temperature, cooling rate, etc.). Verification of properties is by destructive testing on a 'test bend' against specified design requirements[13], with all bends being subject to an identical heating and forming process. These methods are defined in the bending standards, ISO 15590-1 or ASME B16.49.

4.5 Chemical embrittlement mechanisms

There are a number of chemical processes that can cause the toughness of a steel to be degraded. These include *inter alia*, irradiation, contact with liquid metals and take-up of hydrogen into the steel.

Embrittlement mechanisms like these may reduce the toughness of a pressure-containing component *after* it has been hydrotested, so that non-critical defects may become critical during operation, without any incremental change in loading or crack size. Consequently, these effects, where relevant, require very specific attention during design.

The effects of irradiation are relevant in the nuclear industry, but will not affect pipelines apart from those applications.

Contact with liquid metals has caused embrittlement in oil and gas processing plants associated with mercury removal and similar systems, by a process called liquid metal induced embrittlement (LMIE). Liquid metals can also cause rapid and aggressive attack on passivated metals like aluminium and stainless steels, but has a much milder and slower effect on carbon steel, causing embrittlement over time. This is unlikely to affect pipelines downstream of processing plants unless the processing is inadequate to remove liquid metal and prevent it accumulating in the pipeline.

Hydrogen pipelines are susceptible to hydrogen embrittlement. Hydrogen is a small molecule and it can be expected that some of it will dissociate and

[12] High Y/T ratios can also pose a difficulty for high-pressure hydrostatic testing, which is not discussed here.

[13] Because hot bends are short, they are not required to have sufficient toughness for fracture arrest; a fracture can arrest in the straight pipe on either side.

diffuse into steel, accumulating to an equilibrium concentration in the material depending on the pressure of the hydrogen. The result of hydrogen accumulating in the steel is a reduction in initiation toughness and fatigue life, which is understood to be caused by hydrogen congregating at sharp crack tips.

Due to potential application in storage and transportation of sustainably-generated energy, there may be an increase in construction of new hydrogen pipelines around the world. Currently, American design code ASME B31.12 governs design of hydrogen pipelines. The issue of embrittlement is mostly addressed by decreasing the design factor of the pipelines. Further research is being conducted to understand hydrogen embrittlement and its applications for pipeline design. In Australia the Future Fuels Cooperative Research Centre (FFCRC) has initiated a programme of works to further research the issue, and other work is being undertaken around the world.

Chapter 5 : Fracture control objectives for pipelines

5.1 Performance inequalities

The purpose of fracture control is to characterise and limit the failure modes of a pipeline, to reduce the consequence from any uncontrolled threat to the pipeline.

The fracture control objectives for a pipeline are summarised by the inequalities in Figure 5-1, discussed in greater detail below. These have been used in the worked examples in Appendix B.

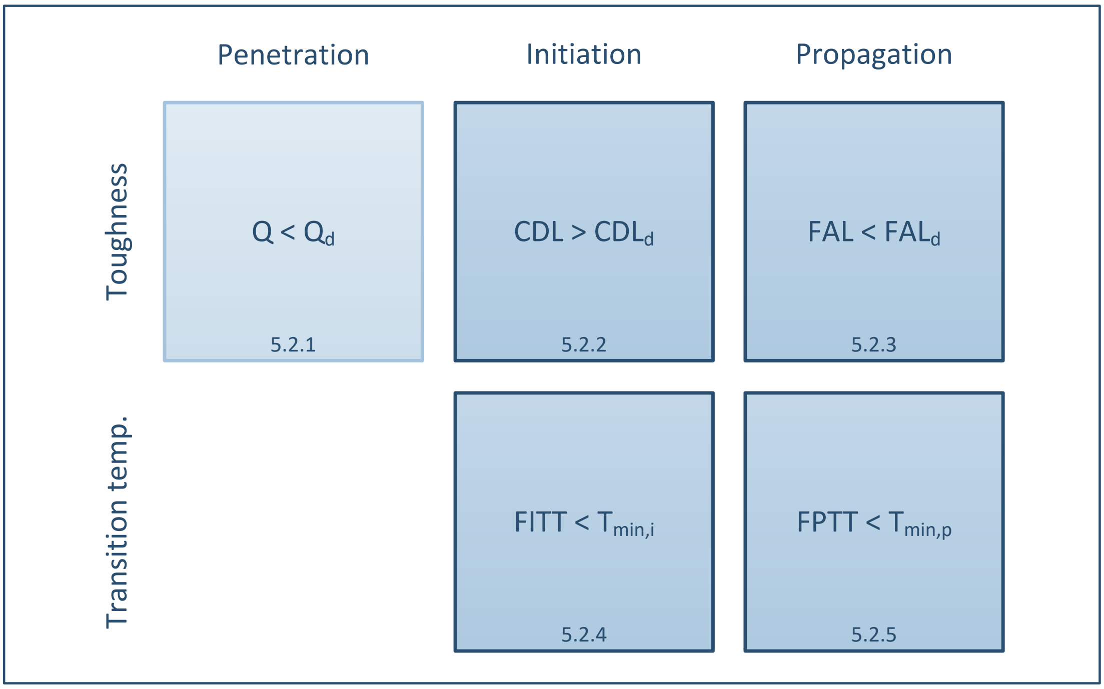


Figure 5-1: The performance criteria in design for fracture control, summarised by five inequalities.

5.1.1 Penetration conditions

Penetration is the formation of a through-wall defect, which can result from several causes:

An *external interference threat*, such as an excavator striking the pipeline, may either create a surface defect (dent or gouge) or may penetrate the pipe and cause a leak. This will depend on the pipe's Resistance to Penetration (RTP), which is a function of wall thickness, t, and ultimate tensile strength, σ_u.

Similarly, a *part-through-wall defect* (existing crack or gouge) may grow to reach a critical depth and length such that it will then break through the pipe wall and create a leak (refer Section 3.2.2).

These two could also occur together, if an external interference creates a part-through-wall defect that exceeds the critical depth and immediately grows to become a through-wall crack.

If a leak is caused in a pipeline, the leak rate will determine the severity of the incident. In a gas pipeline, the greatest threat occurs if the leak ignites, and the *energy* release rate, Q, will determine the size of the hazardous zone around the leak site.

To limit consequence, a designer may nominate a *design* leak rate (Q_d). Achieving this requires control of resistance to penetration, or protection of the pipe from EI threats, to prevent a larger leak from occurring. This results in the performance requirement:

$$Q < Q_d \tag{5-1}$$

5.1.2 Initiation conditions

The fracture initiation toughness of the pipe determines the boundary between a leak and a full-bore rupture, which represents a significant step-change in the safety consequence from a release.

Initiation control is achieved by ensuring the critical defect length for fracture initiation (CDL) is sufficiently longer than the design critical defect length, CDL_d. The CDL_d is determined from the defect size that the most credible event may create, hence reducing the risk of pipeline rupture to an acceptable level, as nominated in the design. The CDL is a function of wall thickness, pressure, strength and toughness, and is calculated using the equations in Section 3.2.

This performance requirement is expressed as:

$$CDL > CDL_d \tag{5-2}$$

5.1.3 Propagation conditions

After a fracture has initiated, it will grow for some distance, and then arrest. The length of the final fracture has implications for the severity of the incident in relation to both safety consequences (the geographical extent of affected area is increased, which increases ignition likelihood) and loss-of-supply consequences (duration of down-time required to effect a repair).

In this document, the fracture arrest length (FAL) is measured in number of pipe lengths (each pipe length will likely be 12 or 18 m, depending on the pipe order, which can be used to convert the FAL to metres if required). Propagation is controlled so that the FAL will be less than a nominated *design* fracture length, FAL_d.

The following performance requirement results:

$$FAL < FAL_d \tag{5-3}$$

In a pressure pipeline, the usual condition for fracture arrest is that the pressure at the crack tip reduces below the pressure required to drive the crack—which requires the depressurisation wave to move faster than the fracture. This is achieved by provision of sufficient material toughness in the line-pipe to reduce the fracture velocity. Achieving arrest toughness in components (such as hot bends and other fittings) is not necessary, because the fracture can arrest in the straight pipe on either side. Calculation of arrest toughness uses the methodology in Section 3.4.

Alternately, arrest could be achieved by periodic installation of crack arrestors or use of mechanical joints, discussed in more detail in Section 6.7.1.

5.1.4 Initiation transition temperature

Initiation control (Section 5.1.2 above) will decrease at low temperatures due to a loss of toughness.

The fracture initiation transition temperature is the boundary between brittle and ductile initiation. At temperatures below the FITT, a defect might fail in a brittle manner. The critical defect size is significantly reduced so that latent defects that were sub-critical during hydrotesting of the pipeline may become super-critical and cause a leak or rupture.

This is prevented by nominating a maximum temperature for the Fracture Initiation Transition Temperature (FITT), such that the pipeline is never cooled below it, except when the pressure is also below the threshold stress required to drive a brittle fracture. This temperature range for effective initiation control should encompass all low-temperature scenarios, including transient conditions. This performance requirement is summarised as:

$$FITT < T_{min,i} \tag{5-4}$$

5.1.5 Propagation transition temperature

If material toughness is being relied on for fracture propagation control (per 5.1.4 above), it is important that long portions of the pipeline are not cooled below the Fracture Propagation Transition Temperature (FPTT). At temperatures above the FPTT a propagating fracture will be ductile. At temperatures below the FPTT it will be brittle or transitional and hence propagate at a higher velocity.

A maximum temperature for the FPTT is nominated. It is required that fracture propagation control is effective down to this temperature, creating the following performance requirement:

$$FPTT < T_{min,p} \tag{5-5}$$

It is recommended that this needs only to encompass the normal operating temperature range. See Section 5.3.

5.2 Causes of defects

Defects in a pipeline can result from a variety of threats, including primarily external interference and deterioration mechanisms such as corrosion and fatigue.

5.2.1 External interference

External interference is a significant potential source of new defects in a pipeline. This is when mechanical equipment, such as excavators, drilling machines, rippers and similar, make contact with the pipe and cause damage to it.

The largest hole that can be made by excavators, rippers and other surface equipment that have teeth, relates to their maximum tooth dimensions. AS/NZS 2885.1 Appendix E provides a calculation method to determine what machinery is capable of penetrating the wall of a pipe.[1]

For augers and horizontal boring machines, the size of defect that is likely to be created is more difficult to determine, but reasonable estimates can be made from geometry if the angle of attack is known—such as in the case of vertical bores. Research is currently being undertaken to gain improved understanding of the damage that may be inflicted by rotating machinery and particularly Horizontal Directional Drilling (HDD) machines.[2]

[1] Due to significant uncertainties, the method includes a “B-factor”, B, which serves as a confidence / safety factor. Where the calculation is used for “No Rupture” design, the B-factor is recommended to be 1.3, in remote areas with very low-consequence, 0.75 may be used. In other situations, 1.0 can be applied.

[2] Energy pipelines co-operative research centre, project 6.3-07.

5.2.2 Deteriorating defects

At the time of construction, the condition of a pipeline is proven by hydro-testing. Hydrotesting the pipeline for 2 hours at 25% above the Maximum Allowable Operating Pressure (MAOP) demonstrates with a margin of safety on pressure that there are no super-critical defects in the pipe.

After construction, defects can be created or worsened due to deterioration mechanisms. General corrosion, both internal and external, cause wall thinning and stress concentration which may eventuate in failure. However, from a fracture perspective the notable deterioration mechanisms are Stress Corrosion Cracking (SCC) and fatigue, which both create sharp defects that will fail when they have grown (in length and/or depth) beyond the critical size.

Both SCC and fatigue are slow crack-growth mechanisms that will usually not impact a pipeline in the first few years of operation:

- *SCC* exhibits as a cluster of part-through-wall defects, which can coalesce into a chain of defects that may eventually exceed the critical length for a part-wall defect. There are a range of risk-factors for SCC, which include high stress, cycling, elevated temperatures, and older coating methods.
- *Fatigue mechanisms* are capable of creating new defects over long periods of time, but more easily will act to grow existing defects, such as latent construction defects. Fatigue is rare in gas pipelines, because the compressibility of the gas limits the rate at which the pressure is able to change, and consequently limits the number of hoop-stress cycles that the pipeline can see over its life[3]. However, gas pipelines that are operated in a 'pack-and-deplete' regime may see operating cycles that will contribute significantly to fatigue damage. Pipelines are not usually intended to be exposed to full pressurisation cycles (from empty to MAOP), so pipelines that have been completely blown down and re-pressurised many times may also be at risk of fatigue damage.

It may be that the operation of stress cycles to cause fatigue can be expected to cause "leak before failure". That is, a defect grown by fatigue will break through the pipe wall, causing a leak, before it will cause sufficient axial

[3] On newer pipelines that are made using higher design factors and higher strength materials, both the mean stress and the stress-cycling amplitude are similarly increased, and so these may see more fatigue damage than historically was expected on gas pipelines.

growth to cause a rupture. The conditions for this can be assessed using the part-through-wall CDL theory in Section 3.2.2[4].

5.2.3 Integrity management

After construction, the integrity management process for the pipeline comes into effect and is intended to ensure that defects are detected and repaired before they become critical. The principle applied for integrity management of these kinds of defects is:

1) Inspect the pipeline and create a database of defects.
2) Dig up the pipeline at strategically selected locations to validate the inspection data.
3) Estimate the growth rate of defects. If more than one inspection has been conducted, then this estimate may be validated by comparing with previous inspection results (giving due regard for inspection tolerances and for improvements in sensitivity of inspection technology).
4) Determine when identified defects are expected to become critical defects, and set a repair schedule, with a margin of safety.
5) Determine when the next inspection will be required (typically 5 or 10-yearly).

The inspection frequency should be sufficient that the minimum detectable defect should not be able to grow into a critical defect between one inspection and the next. If a pipeline has low toughness, then the critical defect depth may be small. Consequently, the margin between a detectable defect and a critical defect is reduced and the inspection frequency should be increased.

From a fracture control perspective, it is important to note that not all pigging technology can detect sharp defects. Generally ultrasonic tools or electro-magnetic acoustic transducer (EMAT) are required where sharp defects are credible.

[4] The leak that may result from an SCC or fatigue threat is generally a narrow slit, so unless it ruptures, it will exhibit a low leak rate. Consequently, consideration of energy release rate, per Section 5.1.1, is usually limited to external interference threats, which create larger leak holes.

5.3 Causes of low temperature

5.3.1 Normal operation

Pipeline temperatures during normal operation result from the following:

- *Source temperature* – the temperature of the fluid source flowing into the pipeline. Several effects may cause high or low source temperatures. Steady-state inlet temperatures may
  - be elevated due to gas compression,
  - flow at elevated temperatures from buried reservoirs (for raw gas / oil production flowlines),
  - be low due to pressure reduction (such as regulation in the distribution pipeline sector), or
  - be low due to other upstream processing (such as LNG regasification or low-temperature separation (LTS)).
- *Ambient temperatures* – if the source is an above-ground facility, then the fluid entering the pipeline may be equalised with ambient temperatures; the duration of this effect would depend on how long the fluid has been above-ground.
- *Soil temperatures* – over some distance, which can be calculated through heat exchange simulation, the pipeline and its contents will equalise with the surrounding soil temperatures. Australian soil temperatures are generally between 10 and 40 °C, depending on the season and the location's latitude.

5.3.2 Transient low temperatures

Transient low steel temperatures in pipelines are generally caused by Joule-Thompson cooling or, in High Vapour Pressure Liquid (HVPL) pipelines, adiabatic vaporisation.

Where the reduction in temperature is sudden, it can also induce tensile stress on the inner surface of the pipe, due to thermal gradients through the pipe wall. This is called "thermal shock"; it is a rare condition that can cause inner wall crack initiation or growth.

As identified above, the minimum temperature for fracture initiation control is commonly estimated by:

1) Determining the minimum pressure at which the hoop stress in the pipeline exceeds the threshold for brittle fracture initiation (typically taken as 85 MPa in Australia), then

2) Determine the minimum temperature that will occur when depressurising the fluid from MAOP at minimum *operating* temperature to the pressure calculated in (1).

If this temperature is very low, a designer can either use material with a low transition temperature, opt for procedural control of low temperatures (such as undertaking staggered pressurisation) or do a more rigorous analysis of the transient low-temperature scenarios. These scenarios include:

5.3.2.1 Pressurisation.

When a gas pipeline is filled from a high-pressure source, the gas entering the pipeline is cooled due to Joule-Thompson effect. For a HVPL pipeline, cooling may also result from adiabatic vaporisation in the source, depending on the pipeline filling and pressurising process.

Even though the gas itself undergoes significant cooling during the early stages of pressurisation, the pipeline steel is unlikely to see the same temperatures. There are several effects that are relevant:

1) the pipeline's thermal mass,

2) heat from the surrounding soil,

3) the time taken to pressurise the pipeline, and

4) the pressure differential diminishes over time.

By the end of the filling process, the gas that was introduced at the start is now being pressurised by the new gas flowing in, which causes an increase in its temperature, and it is likely that at the end of pressurising, the pipeline will be *hotter* than the gas source.

The lowest temperatures calculated will only apply close to the fill point, where the velocity of the cold gas sustains convective heat transfer. For this reason, it is common, on new pipeline designs, to use low-temperature materials for the filling line and particularly where the flow chokes, but not for the whole pressurising facility.

5.3.2.2 De-pressurisation.

De-pressurisation in a gas pipeline can cause low temperatures both up- and down-stream of the choke point. Upstream, the expansion of the gas in the pipeline causes Joule-Thompson cooling, but it is gradual (usually taking several hours), and heat flow from the pipe and surrounding soil will prevent the temperature from becoming much lower than the initial temperature.

Across the choke point, a more sudden drop in pressure can cause a greater and immediate drop in temperature, so the vent piping will see lower temperatures than the pipeline. However, depending on the magnitude of back-pressure in the vent system, these temperatures are not coincident with high pressure and so commonly will not coincide with operating stress exceeding the threshold stress for brittle fracture.

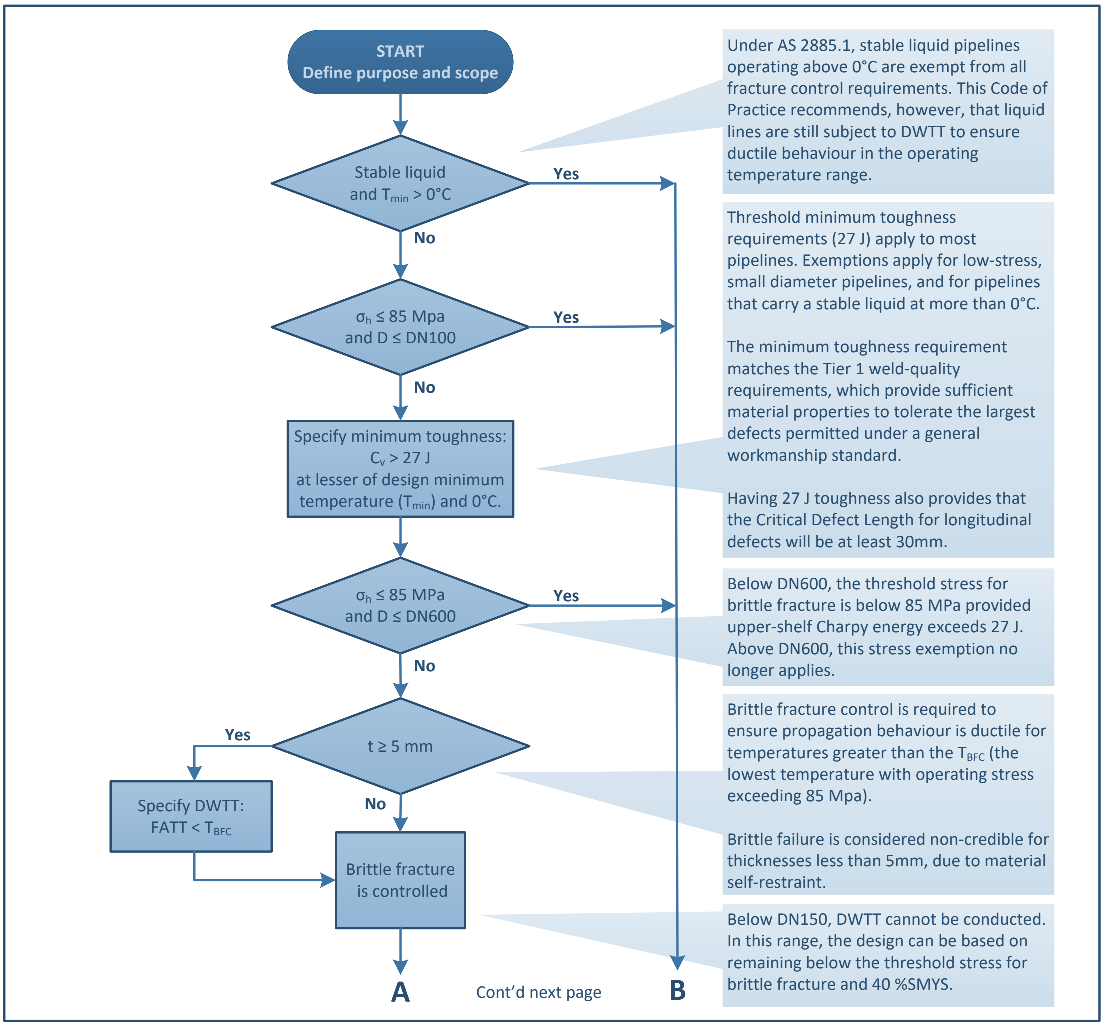
Furthermore, research by the EPCRC is now available to estimate metal temperatures that will occur in these scenarios, refer EPCRC project RP3-12, *Pressure and thermal transients*.

Chapter 6 : Australian Standard AS/NZS 2885.1

Australian high-pressure petroleum and gas pipelines are designed to Australian Standard AS/NZS 2885.1. The standard provides a methodology to design a new pipeline for fracture control, in Clauses 4.9.2 and 5.3 of the 2018 edition.

The standard requires that all pipelines have a Fracture Control Plan (FCP), which details the fracture control objectives for the pipeline and how those objectives are met by the design.

The fracture control process for common designs is summarised in a decision diagram, reproduced with commentary in Figure 6-1 of this Code of Practice.



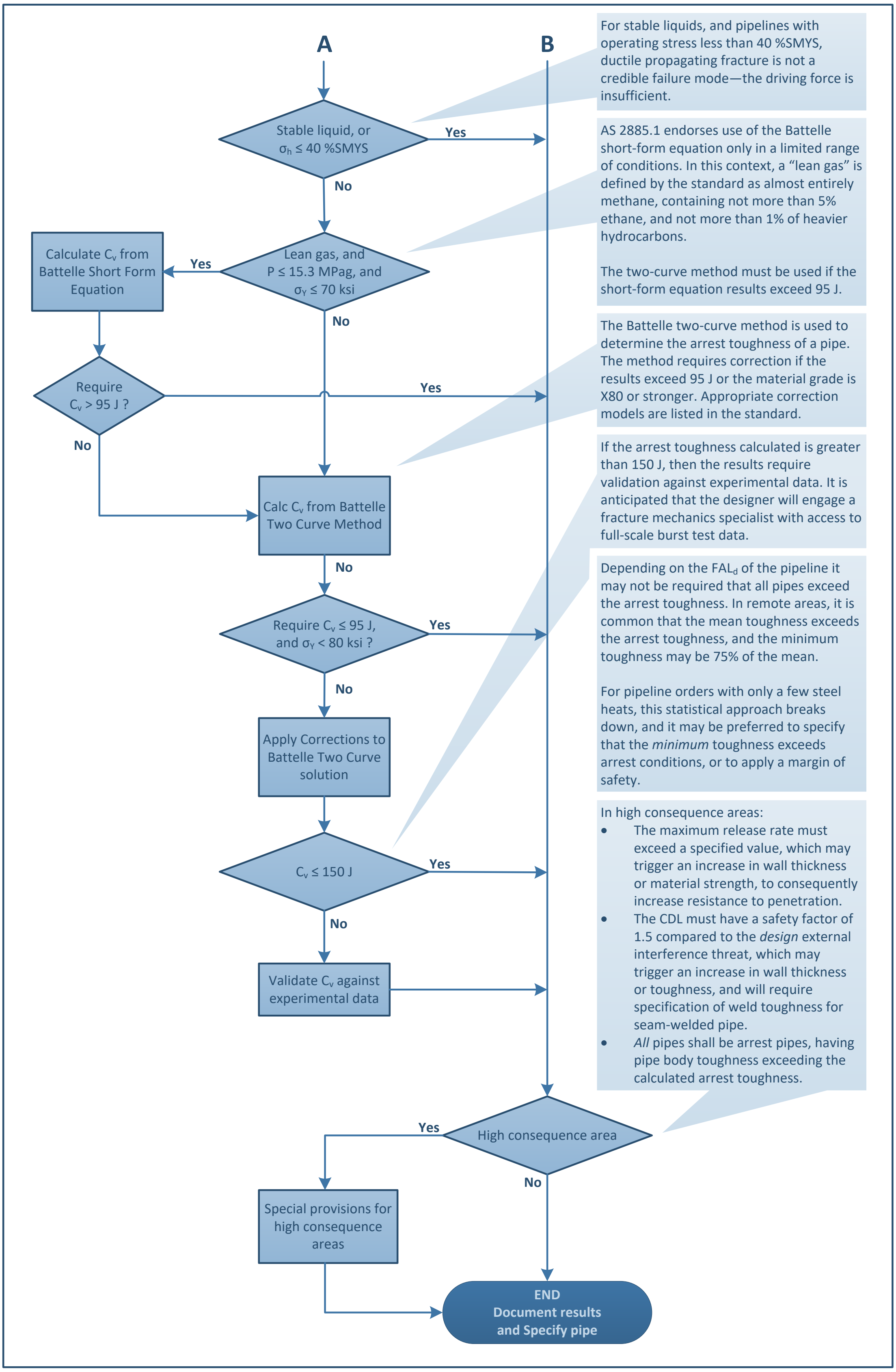


Figure 6-1: The AS/NZS 2885.1—2018 fracture control decision diagram (reproduced from AS/NZS 2885.1 Figure 5.3.2)

The fracture requirements of AS/NZS 2885.1 for new pipelines are summarised in the following sections.

6.1 Baseline minimum toughness

Under AS/NZS 2885.1, most Australian pipelines are required to have a minimum Charpy toughness of 27 J in the longitudinal crack direction (transverse specimen), or 40 J if measured in the transverse crack direction (longitudinal specimen). The toughness is measured at the lesser of the design minimum temperature, T_{min}, and 0°C. In this case T_{min} is commonly based on the operating minimum temperature, not transient low temperatures. Baseline toughness criteria are summarised in Figure 6-2.

This requirement applies to the mainline pipe, and any components directly welded to the mainline pipe. It does not apply to stations or any pipeline assemblies that have been designed in their entirety to another standard (ASME B31.3 or AS 4041).

6.1.1 Justification

Twenty-seven Joules has been in use by the pipeline industry for several decades. It is not derived from a mathematical basis but is an effective benchmark to screen out poor steels that may exhibit low toughness in the design temperature range. 27 J is a unit conversion of 20 ft-lb, which historically was specified in several ASME codes. This toughness was then adopted into API 5L as the baseline toughness requirement for PSL2[1] line-pipe and in turn by AS/NZS 2885.1[2] to enable use of off-the-shelf API 5L line-pipe.

This requirement achieves the following:

1) Sets an effective benchmark for minimum steel quality, screening out low-toughness steels, and effectively guaranteeing upper-shelf or transitional fracture *initiation* behaviour.

2) Complements girth-weld defect tolerance requirements. Weld procedures developed to AS/NZS 2885.2 under Tier 1 requirements, require a weld toughness of 27 J, so that the material can tolerate permissible weld defects resulting from undercut, misalignment and similar. This requirement ensures that the base metal has similar toughness to that required of the weld.

[1] PSL – Product Specification level. Under API 5L, PSL1 pipe does not require toughness testing, whereas PSL2 requires 27 J minimum toughness, unless otherwise specified.

[2] It is worth noting that European pipeline standards have adopted 40 J as a baseline minimum toughness.

3) Ensures that the CDL is at least 30mm.[3] This is not the primary reason for minimum toughness requirements, but typically line pipe steels that have 27J toughness will have a CDL of at least 30mm.

6.1.2 Specimen orientation

Setting a different magnitude of toughness requirement in the case of longitudinal specimen testing aligned with API 5L when it was first introduced. Subsequently API 5L has removed this requirement, but it has been retained as an option in AS/NZS 2885.1.

The toughness difference (40J compared to 27J) is due to potential toughness anisotropy in the steel. Longitudinal specimens may exhibit higher toughness than transverse, due to the steel manufacture method. The requirement in AS/NZS 2885.1 will ensure the transverse toughness is still greater than 27 J, provided the anisotropy index[4] is less than about 0.3, which is likely. However, due to the availability of gull-wing testing, Charpy testing on longitudinal specimens is usually unnecessary and it is not recommended.

6.1.3 Exemptions

Under AS/NZS 2885.1, exemptions from this requirement apply in the following two circumstances:

- The pipeline is carrying a stable liquid at greater than 0°C.

  Pipelines carrying stable liquids have a different risk profile than gas pipelines, because the stored energy is less. Often the environmental risk is greater than the safety risk, and for flammable liquids, the release *volume* is more important than the release *rate* (in contrast to flammable gases). Exemption from the minimum toughness requirement enables a pragmatic, risk-based approach to fracture control on pipelines where the consequence of rupture may not be very high.

  Minimum toughness is still required when the design minimum temperature is less than 0°C. This is an arbitrary cut-off, ensuring that low-temperature design triggers the use of low-temperature steels. Note that operating temperatures below 0°C are rare in Australian pipelines.

- The pipe diameter is DN100 or smaller, and the hoop stress at the maximum allowable operating pressure (MAOP) is less than 85 MPa.

[3] This is based on calculation using Equation (3-4) for a wide range of credible pipe designs.

[4] Anisotropy index for variance of a material property, A, between two perpendicular axis is defined as (Amax – Amin) / Amax.

> The threshold stress used provides a conservative range in which rupture is not plausible, even at low toughness. In pipes that are DN100 and smaller, 85 MPa is much less than the threshold stress for propagating brittle fracture, even down to 2 J CVN toughness. For all pipe materials permitted under AS/NZS 2885.1, 85 MPa is also less than 40 %SMYS, which means that propagating a ductile fracture is not credible either (Refer Section 3.4.2).

For pipelines exempt from Charpy testing, the exact critical defect length (CDL) cannot be calculated. In these cases, it is acceptable to use a conservative assumption of toughness, such as 5 J, to determine a lower estimate of CDL. This is a suitable assumption for new materials used under the standard, but such assumptions should be justified for legacy pipelines in retrospective application.

6.1.4 Alternative toughness-temperature requirements

Where the minimum baseline toughness requirement applies, it rules out the use of several pipe and fitting grades that are otherwise commonly used in Australia, namely, ASTM A106 GrB, A105 and A234 WPB, unless supplementary toughness testing is completed.

Other common materials are Charpy tested to less than 27 J, but at a lower temperature (such as ASTM A333 Gr 6). For these, the full-size equivalent Charpy toughness may be adjusted by 1.5 J per Kelvin between 18 and 50 J, to approve their use in components and assemblies. This conversion is borrowed from Australian Standard AS 4041.

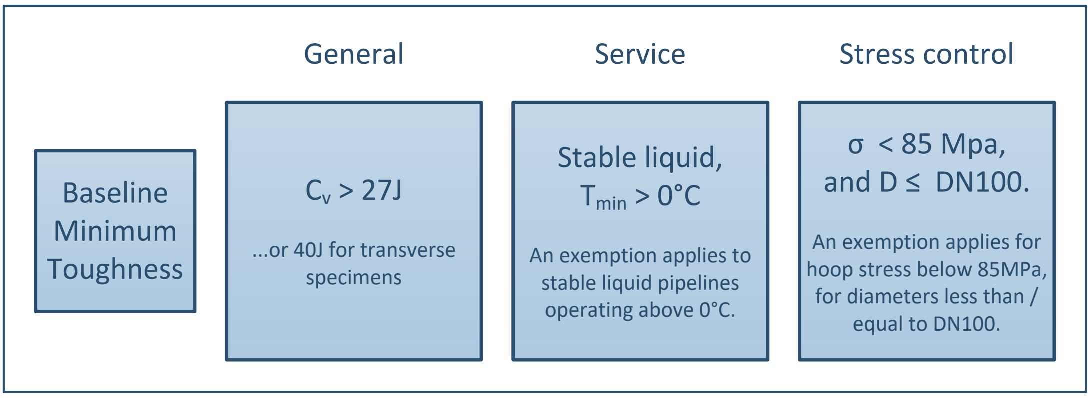


Figure 6-2: An overview of the AS/NZS 2885.1 baseline toughness criteria.

6.2 Brittle fracture control

Brittle fracture control is control of the steel ductile-brittle transition temperature. Brittle control requirements are summarised in Figure 6-3.

Currently, AS/NZS 2885.1 requires that propagation is controlled down to a nominated Temperature for Brittle Fracture Control (T_{BFC}). The temperature for brittle fracture control is any temperature that the pipe may see coincident with hoop stress greater than 85 MPa. Because propagation is controlled to this temperature, initiation is implicitly controlled also, because fracture initiation has a lower transition temperature than propagation.

The fracture propagation transition temperature is approximated by the 85% shear fracture appearance transition temperature (FATT, T_w) from Drop-Weight Tear Testing (DWTT)—refer Section 4.2.2.

6.2.1 Alternative controls

There are three exemptions for this requirement[5]:

- Any pipe that is thinner than 5mm is exempt from meeting this requirement on the basis that, due to low restraint, the transition temperature can be relied on to be very low for materials thinner than 5mm. This reasoning applies to materials accepted under the standard, and may not be valid for legacy materials.
- Pipelines carrying a stable liquid above 0°C are exempt from brittle fracture control under AS/NZS 2885.1. However, this report recommends that this exemption should not be continued. Refer Section 6.2.3 below.
- Pipelines up to DN600 that have an operating hoop stress less than 85 MPa are exempt from brittle control (calculation shows that above DN600, with 2.7 J toughness, the threshold stress for brittle fracture may be lower than 85 MPa—see Figure 3-3).

  It is implied that above DN600, if the operating stress is less than 85 MPa, then T_{BFC} should be taken as the design minimum temperature, though this has not been enunciated in the standard.

[5] Note that any pipelines exempt from having 27 J minimum toughness are also exempt from brittle fracture control under these requirements.

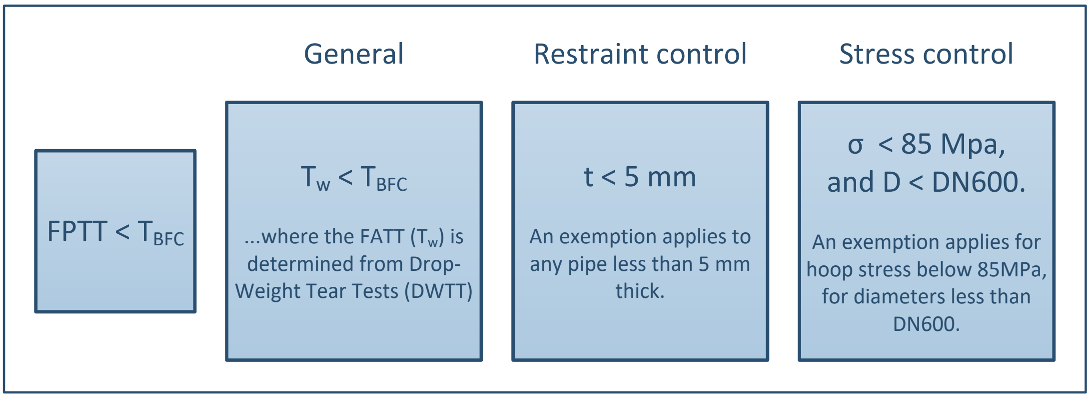


Figure 6-3: An overview of the AS/NZS 2885.1 design criteria to control brittle fracture.

6.2.2 Small-diameter pipe

Drop-weight tear testing of pipe is not feasible for pipes smaller than DN300. Down to DN150, it is reasonable to undertake testing on gull-wing specimens, flattened specimens, or the plate or strip from which the pipe was manufactured.

Below DN150, if the pipe is thicker than 5mm, the only option currently available in the standard is to either increase the thickness to reduce the operating hoop stress below 85 MPa, or reduce the thickness below 5mm (having the opposite effect).

However, running fracture in this range is unlikely. This report recommends undertaking a manual calculation of the threshold stress for brittle fracture at either a conservative estimate of lower-shelf toughness, or at the measured lower-shelf Charpy toughness (specified, in this case, at the T_{BFC} rather than T_{min}), which may be used in lieu of 85 MPa up to a maximum of 30% of SMYS.

6.2.3 Brittle fracture control – recommendations

An alternate approach to brittle fracture control is recommended in this report. Though this method is not consistent with the 2018 revision of AS/NZS 2885.1, it is recommended to the standard committee for future revisions and may be applied for *retrospective* fracture control, because it results in fit-for-service conditions. These requirements are summarised in Figure 6-4.

The changes recommended are as follows:

1) That fracture *propagation* control only be required down to the minimum operating temperature, not the minimum transient temperature.

2) That fracture *initiation* control be required down to the minimum transient temperature, and either Charpy V-notch testing is conducted at the minimum transient temperature (or, if applicable correlations

exist for the material, the FITT may be assumed to be a specific margin below the FPTT, refer Section 2.6).

3) That, rather than 85 MPa, the minimum stress for brittle fracture control *may* be taken as the lesser of the threshold stress for brittle fracture calculated according to Section 3.3.1, and 30 % of the SMYS.

4) That, where DWTT has not been or cannot be conducted, the fracture *initiation* transition temperature requirement may be met through Charpy testing, in which the fibrosity (shear area) is reported and is greater than 85%. Charpy results are dynamic, and consequently equal to or higher than the FITT *for the same thickness*. Charpy results on specimens thinner than the pipe wall require to be adjusted for wall thickness. Refer Section 4.2.1.3.

   Note that this approach is acceptable for initiation transition only, not propagation transition.

5) That pipelines that carry stable liquids above 0°C are no longer exempt from brittle fracture control.

Except for item 5, this method is less conservative than AS/NZS 2885.1, and may consequently reduce cost in steel specification for some projects.

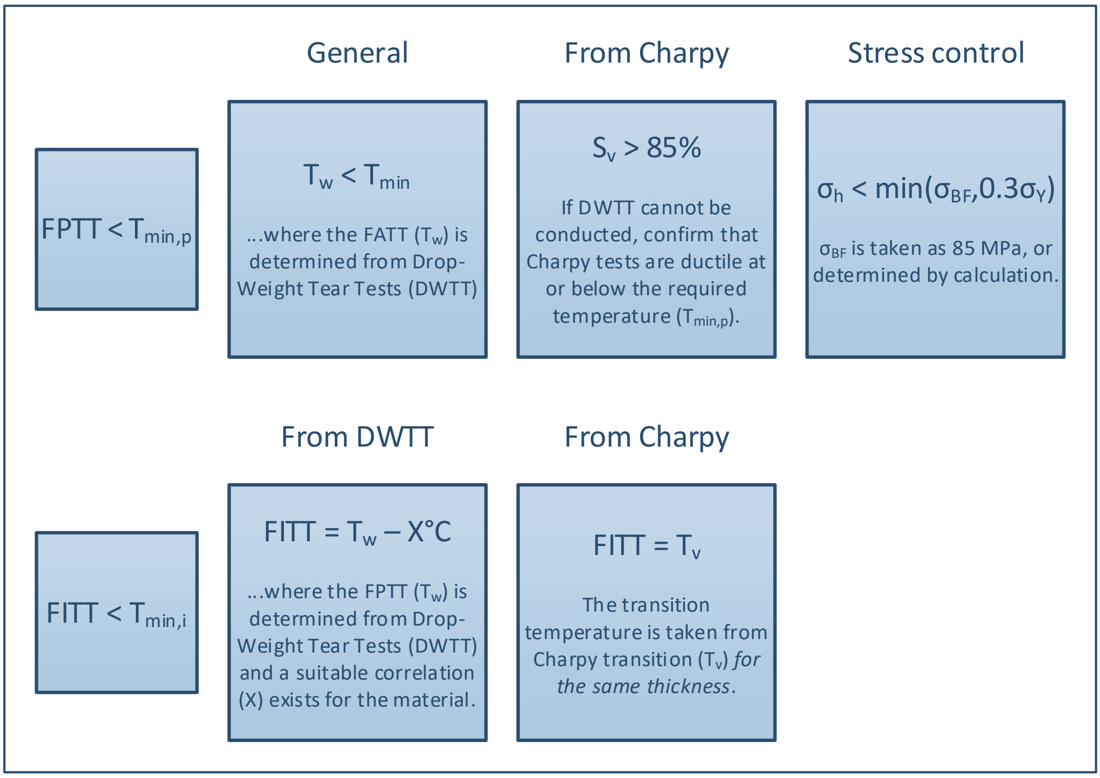


Figure 6-4: An alternate method for controlling brittle fracture.

6.3 Fracture propagation toughness

For gas pipelines, fracture propagation control is also required in the material's ductile range, to limit the total fracture length after rupture. Fracture propagation requirements are summarised in Figure 6-5.

6.3.1 Mandatory FAL_d requirements

In high consequence locations, the design intent is that a rupture should be as localised as possible, and hence the fracture arrest length is limited to one pipe length. This is achieved by ensuring that the pipe body toughness of *every* pipe exceeds the arrest toughness. With every pipe being an "arrest pipe", a fracture can be expected to either arrest in the initiating pipe[6] or, if the fracture initiates in a lower-toughness weld seam, in the pipes on either side (weld toughness is not required to exceed arrest toughness).[7]

In more remote areas, a fracture arrest of five pipe lengths is commonly specified. This is achieved if there are, at most, five consecutive "propagate pipes", that is, pipes with body toughness lower than the arrest toughness. Rather than review the pipe distribution prior to construction, this may be achieved statistically, with 95% confidence, by ensuring that at least half of the pipe lengths in each production run are arrest pipes.

In remote "rural" areas, AS/NZS 2885.1 permits an FAL_d greater than 5, if this is assessed and accepted in the Safety Management Study (SMS).

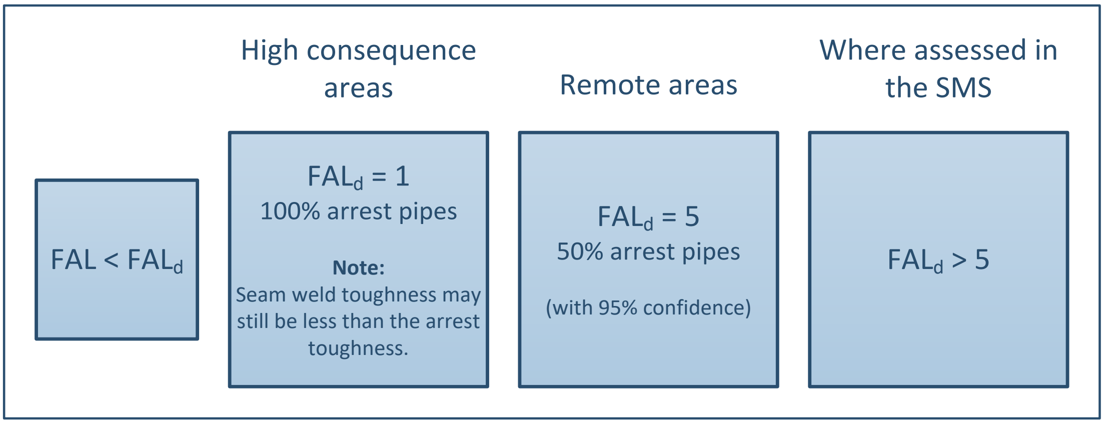


Figure 6-5: An overview of the AS/NZS 2885.1 design criteria to control fracture propagation.

[6] Even if the fracture initiates in the pipe body of an arrest pipe, it may still arrest within adjacent pipes if the margin between arrest toughness and actual toughness is low; even in arrest pipes, arrest occurs over a distance.

[7] Under AS/NZS 2885.1, weld seams are required to be offset between adjacent pipes. Consequently, a fracture cannot travel from one weld seam to another.

6.3.2 Exemptions and simplifications

Ductile fracture control is not required if:

- The pipeline carries a stable liquid. Pipelines carrying stable liquids are not susceptible to ductile propagating fracture, because the decompression speed in such fluids is significantly greater than the fracture propagation speed
- The hoop stress is less than 40% of SMYS. It is broadly acknowledged that ductile fracture cannot propagate in this range. See also Section 3.4.2.

6.3.3 Calculating the arrest toughness

Ductile fracture modelling is still a developing field of knowledge. The arrest toughness is calculated using the Battelle short-form equation or the Battelle Two Curve Method (BTCM), as described in Section 3.4.

Corrections to these methods are required in some circumstances. Currently, the accepted approach is:

1) Use the Battelle short-form equation only when:

    (a) $C_v < 95$ J,[8]

    (b) The composition is lean gas,

    (c) The design pressure, $P \leq 15.32$ MPag, and

    (d) The SMYS ≤ 70 ksi.

2) Otherwise, use the BTCM approach.

    (a) No correction model is required if the BTCM arrest toughness is less than 95 J

    (b) The Leis 1997 correction model is required if the arrest toughness is between 95 J and 150 J

    (c) Multiply the uncorrected result by 1.4 if the SMYS is 80 ksi.

3) The Wilkowski 2000 correction model is required as an *initial estimate* if the arrest toughness is greater than 150 J, then the assessment shall be validated against all available experimental data.

Material grades higher than X80 are not permitted under AS/NZS 2885.1 and are not specifically addressed.

[8] The Battelle short-form equation may be valid with correction factor applied, but this is not currently supported in AS/NZS 2885.1.

When the calculated arrest toughness is greater than 150 J, validation against available experimental data is required. This means using data from full-scale burst tests to confirm that the pipe properties will lead to fracture arrest. In these conditions, a pipeline company is recommended to engage a specialist who has an advanced understanding of fracture control. It is possible that future research will characterise fracture arrest conditions so that this is not required, but currently the established methods of fracture control are not reliable in this range.

6.3.4 Statistical confidence levels

Within a production run of line-pipe, there will be variation in toughness. The toughness will vary between heats, and within a heat due to variations in the manufacturing process. After manufacture, steel heats are effectively randomised each time that the steel is stockpiled and then relocated. There are about seven instances of re-ordering between the steel mill and the pipeline construction lay-down area, which result in the distribution of heats being random along the pipeline ROW.

Due to the random distribution, it is accepted to use statistical methods to estimate the fracture arrest length (FAL), as defined[9] in AS/NZS 2885.1, with about 95% confidence. If p is the proportion of pipes that are "propagate" pipes, then the probability that there will be N or less *consecutive* propagate pipes, is:

$$P(FAL \leq N) = 1 - (N+1)p^{N+1} + Np^{N+2} \tag{6-1}$$

Note that if p = 0.5, then P(FAL ≤ 5) ≈ 95%, which is the basis for requiring 50% propagate pipes in remote areas.[10]

Though the FAL is five, it may require six or seven pipe lengths to affect a repair, because the fracture has actually arrested in the pipes at either end. This needs to be considered when assessing supply consequence in the Safety Management Study.

[9] The definition of FAL in AS 2885 is the number of consecutive propagate pipes: $FAL = N$, not the actual final length of the fracture. This definition was adopted in the 2018 revision, to minimize changes made to the approach applied by previous editions of AS 2885.1, which required 50% of pipes to be arrest pipe. Other references count the pipes at either end as part of the fracture length (i.e. $FAL = N + 2$).

[10] This formula accounts for toughness in the initiating pipe. For some failure mechanisms, there is a reasonable likelihood that fracture will initiate in the weld seam. Where weld seam toughness is not sufficient for arrest, the initiating pipe may be discounted with the following alternate formulae: $P(FAL \leq N) = 1 - Np^{N+1} + (N+1)p^{N+2}$.

6.3.5 Specification of line-pipe

When FAL_d = 1 and a fracture is expected to arrest in the initiating pipe, the *Charpy toughness (any test unit average)* is required to be greater than the arrest toughness.

When FAL_d = 5, the *Charpy toughness (all test unit average)* should be greater than the arrest toughness.

AS/NZS 2885.1 also recommends a 'statistical factor' of 0.75. As defined in the standard, this means that the minimum toughness of any test unit in a production run should be not less than 0.75 times the mean toughness of all pipes. This limits the spread of Charpy toughness results.

For order with less than 6 test units, this method is considered inadequate, and the minimum, rather than the mean, toughness shall be specified (i.e. the statistical factor = 1.0). For small orders, also consider guidance in Section 4.3.1.

6.4 High consequence areas

High consequence areas are locations where the safety or environmental consequence of a pipeline rupture is high, thus warranting extra effort in the design to prevent it.[11] Figure 6-6 and Figure 6-7 summarise the methods to meet high consequence area requirements.

High consequence areas have the following set of mandatory requirements:

- As per Section 6.3.1 above, in high consequence areas every pipe shall be an arrest pipe, so the pipe body toughness must exceed the arrest toughness in every pipe—i.e. FAL_d = 1. (AS/NZS 2885.1 Clause 5.3.2)
- "No rupture" requirements apply, meaning that the CDL must exceed the largest defect with a safety factor of 1.5. (AS/NZS 2885.1 Clause 4.9.2)
- An energy release rate limit applies in gas pipelines, for an ignited leak resulting from the largest credible defect. (AS/NZS 2885.1 Clause 4.9.3)

6.4.1 Location classification

AS/NZS 2885.6 Section 2 defines four primary location classifications and six secondary classifications, shown in Table 6-1. High consequence area requirements apply to all T1, T2, S (designed to T2) and I (designed to T1) locations and *may* apply to E and HI locations if deemed applicable in the SMS.

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[11] Designers are required to consider any features with the 4.7 kW/m^2 radiation radius of a full-bore rupture at MAOP to determine the classification.

The requirements for location classes are summarised here with respect to fracture control requirements only. Refer to the Standard for full details.

Table 6-1 : AS/NZS 2885.1 location classes

Primary Location Class		Secondary Location Class	
R1	Rural	S	Sensitive
R2	Rural Residential	E	Environmental
T1	Residential	I	Industrial
T2	High Density	HI	Heavy Industrial
		CIC	Common Infrastructure Corridor
		C	Crowd

6.4.2 "No rupture" pipe

As part of the safety management process for any pipeline, threats to the pipeline are analysed to determine whether they are capable of penetrating the pipe wall and creating a loss of containment event or not. If the resulting longitudinal defect exceeds the length of the CDL, then the consequence is rupture, whereas shorter defects will result in a leak, the less severe outcome.

In any designated high-consequence area, AS/NZS 2885.1 mandates the use of "no rupture" pipe. The designer is required to define a *design* external interference threat based on what equipment is reasonably likely to be used in the area. The CDL must then be 1.5 times the defect that would result from the design threat.

In *high consequence areas*, the design CDL must be achieved in both the pipe body and in the pipe's longitudinal weld seam (if there is one). This means that the designer must specify weld seam toughness for seam-welded mainline pipes. Mandatory requirements *only* apply to the design Critical Defect Length in *high-consequence areas*.

6.4.3 Energy release rate

If a leak is caused in a gas pipeline and the gas ignites, the energy release rate, Q, will determine the severity of the incident.

Designers are required to consider all threats that could result in a loss of containment. In residential (T1) locations, the release rate shall be less than 10 GJ/s, and in high-density (T2) locations, the release rate shall be less than

1 GJ/s. (Mandatory requirements only limit the permissible energy release rate in *high consequence areas*.)

This means that even when the CDL requirements are met, the designer may need to increase the wall thickness to increase the material's resistance to penetration and reduce the likelihood of a leak.

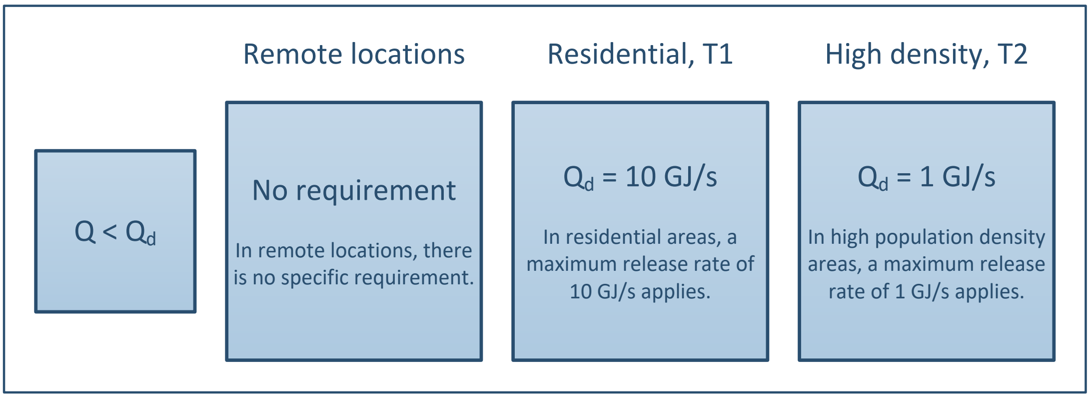

Figure 6-6: An overview of the release rate limits under AS/NZS 2885.1.

6.5 Good practice design for fracture initiation

Outside of high consequence areas, no specific mandatory fracture initiation controls apply for mainline pipe.

However, the Safety Management Study process is used at all locations to determine that the pipeline's risk profile is acceptable. This may warrant an increase of the CDL due to higher specified toughness or wall thickness (reduced hoop stress) if the likelihood of rupture is too high to achieve acceptable risk.

It is good practice to:

1) Compare the CDL to any external interference threats that can penetrate the pipe, and consider changing the design to increase the CDL if doing so is a cost-effective means of increasing pipeline safety.

2) Compare the CDL to the maximum CDL that could be achieved at high toughness. If the CDL could be significantly increased (more than 10 to 20% increase) by increasing the toughness alone, then this is likely to be a cost-effective way to make the pipeline safer.

Increasing the pipeline wall thickness can be very expensive, whereas increasing the toughness within the commonly achievable toughness range (< 100 J full-size equivalent CVN) is not likely to add significant cost to a line-pipe order, though it is not possible for ex-stock pipe orders manufactured to a lower toughness practice.

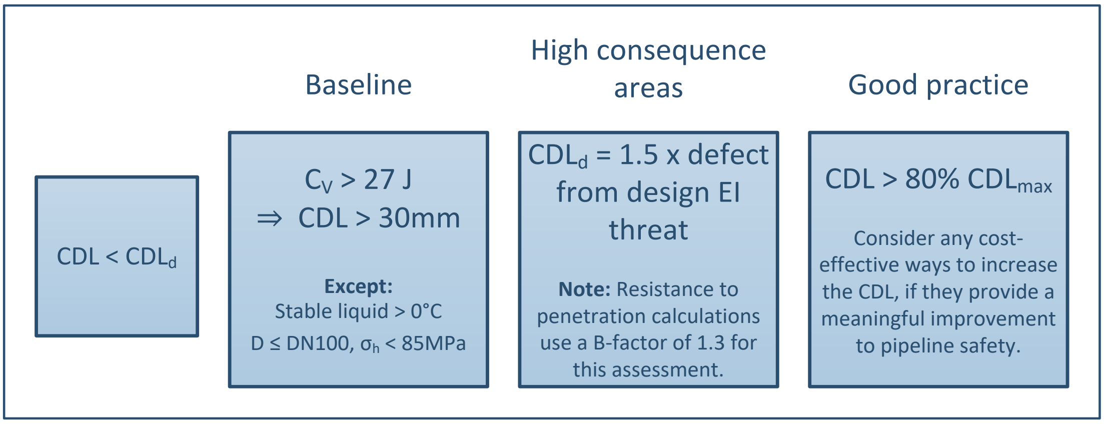

Figure 6-7: An overview of the methodology to design for fracture initiation.

6.6 Safety Management

Pipelines are required to have a Safety Management Study, in which threats to the pipeline are identified, assessed and controlled. As discussed above, mandatory fracture requirements exist in "high consequence" locations. In other locations, the SMS process can be used to nominate the CDL_d and FAL_d (and Q_d, if relevant) for the pipeline.

This document is not focused on Safety Management Studies, and includes only details relevant to fracture control and providing context for retrospective fracture assessment covered in the next Chapter.

6.6.1 Safety management process

AS/NZS 2885.6 provides the authoritative and complete description of the SMS process, which is also summarised diagrammatically in Figure 6-8. The general process is to identify threats, control those threats, and then—if the threats are not controlled and result in a loss of containment—assess what the consequence will be, what is the likelihood of that consequence occurring, and the risk ranking.

The consequence of a failure is assessed with respect to three outcome categories:

- *Safety consequence* occurs when an incident presents a risk of injury or fatality to people. The more people there are near the pipeline, the worse the safety consequence may potentially be. For high-pressure gas pipelines, a rupture is explosive in nature due to the expansion of the gas, which poses the first safety threat. For flammable contents, risk of ignition will *increase* the maximum potential consequence to the size of the zone in which temperatures or radiation intensity exceeds safe levels for people.

- *Environmental consequence* applies if there is any direct risk to the environment resulting from an incident. Generally the environmental risk from a gas release is not significant (especially if the gas is buoyant). However, for oil and other liquid pipelines, the size and location of the leak and especially the proximity of the incident to natural waterways or other environmental vectors will determine the magnitude of potential environmental impact from a release.
- *Supply consequence* occurs when downstream customers of the pipeline rely on the pipeline for critical functions. Where there are redundant or alternate supply options, this risk is reduced. If the pipeline is the sole supplier of energy to a township, in particular a hospital, critical power generator or similar infrastructure, the supply risk can be significant.

Note that the SMS process is not required to consider *economic* impacts to the pipeline owner or downstream consumers, but most owners will consider these in parallel to the SMS, as the cost of any potential down-time is an important design input.

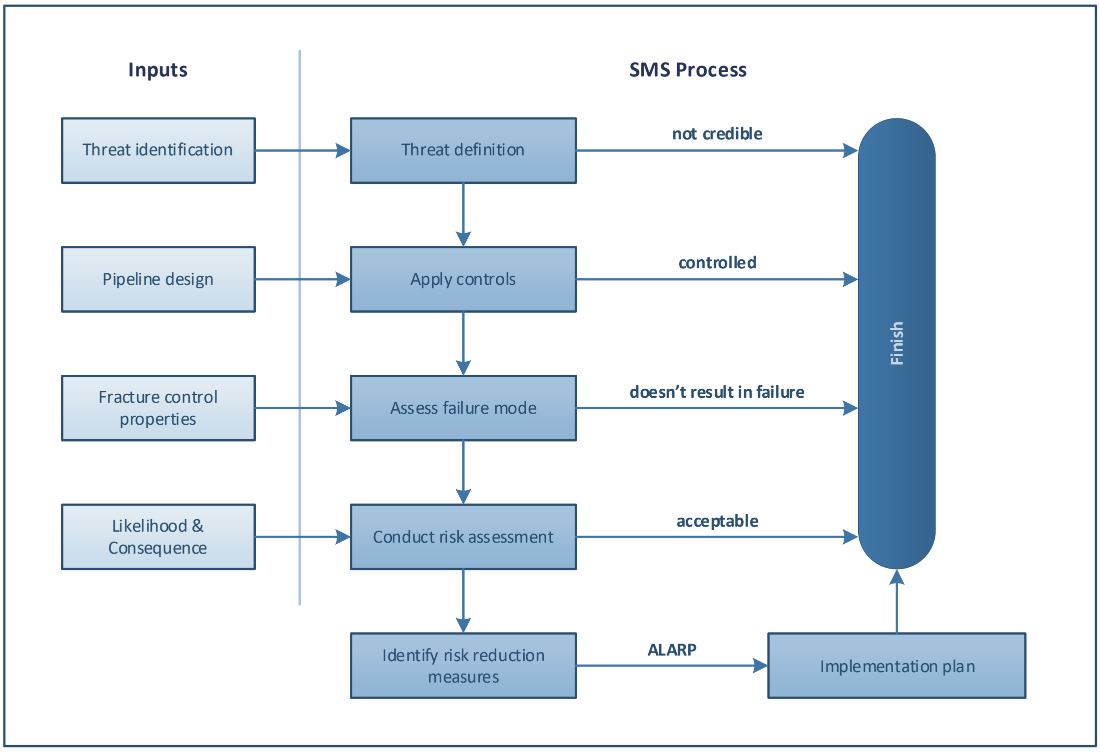

Figure 6-8: An overview of the SMS process. Reviewing each threat through SMS can be used to determine the CDI_d and FAL_d for the pipeline.

Fracture mechanics is relevant at the point of assessing the failure mode of the pipeline. The fracture properties of the pipeline determine whether a defect will leak[12], whether a leak will rupture and whether a rupture will propagate.

Even if all threats are controlled on a pipeline, AS/NZS 2885.6 recommends a "control failure check". This acknowledges that, even on a pipeline where all identified credible threats have been assessed as controlled, there remains a remote possibility that an incident will occur despite controls and cause a leak or a rupture on the pipeline. The consequences of such an incident should be known and reduced if practicable.

6.6.2 Risk assessment categories

AS/NZS 2885.6 defines a risk assessment process with five consequence and likelihood levels, and five resulting risk rankings. They are summarised in Figure 6-9; refer to the standard for an authoritative definition.

Likelihood \ Consequence	Catastrophic	Major	Severe	Minor	Trivial
Frequent	Extreme	Extreme	High	Intermediate	Low
Occasional	Extreme	High	Intermediate	Low	Low
Unlikely	High	High	Intermediate	Low	Negligible
Remote	High	Intermediate	Low	Negligible	Negligible
Hypothetical	Intermediate	Low	Negligible	Negligible	Negligible

Figure 6-9: Summary of AS/NZS 2885.6 frequency, consequence and risk categories.

6.7 Alternate means of fracture control

For most pipeline designs, fracture is controlled by provision of sufficient material toughness across the applicable temperature range, which is reflected in the performance requirements above. In achieving this, designers will most commonly adjust either the wall thickness or the material toughness.

However, there are alternate means that may be used to control fracture in some circumstances.

[12] Resistance to penetration is not technically a fracture mechanics problem, and it is not a function of fracture properties. However, it is essential to include in the broader consideration of a pipeline's failure modes.

These include:

- *Mechanical crack arrestors.* These are devices that will arrest a crack. Generally, they are regions of pipe with greater wall thickness. Other types wrap around the pipe and relieve hoop stress, hence decreasing the driving force on a crack. Crack arrestors should have proven effectiveness for the service and crack type (ductile or brittle).
- *Mechanical joints.* Mechanical jointing methods, such as bolted flanges and press-fit connections (e.g. Zap-Lok), will arrest fractures, because a crack cannot grow from one piece of steel to another unless they are contiguous (welded).

Fracture behaviour may also be addressed by changing the operating conditions, through:

- *Pressure reduction.* Generally a designer cannot change the operating pressure as it is a function of the hydraulic properties of a pipeline. However, at the concept stage, it is possible to influence the operating pressure, such as by moving pressure reduction stations further upstream in a distribution system.
- *Temperature control.* Where transition temperature is a limiting concern for a pipeline, controlling low temperatures may be possible, such as by means of pre-heating at pressure reduction stations, through use of multiple-stage pressure cuts and temperature recovery between them, or by procedural control during pressurising.
- *Composition control.* In rare occasions, controlling the composition of a pipeline (e.g. to supress the vaporisation pressure) may be an effective means of controlling propagating fracture.

Finally, for pipelines subject to fatigue risk, a programme of re-hydrotesting can be implemented. The rationale of this method is two-fold: Firstly, the hydrotest ensures that super-critical or near-critical defects have not formed in the pipeline. Secondly, hydrotesting applies an over-stress cycle to existing defects, which can increase their fatigue life[13] through crack blunting—this has to be applied carefully with reference to suitable standards. Hydrotesting is accompanied by other risks, such as corrosion from water, over-pressure due to elevation differences, and possibility of pipeline failure with associated repair burden and increased downtime.

[13] The over-cycle can create a large plastic zone at the crack tip, which is relieved on unloading, leaving a compressive residual stress at the crack tip, slowing down the crack's progression. However, subsequent under-cycles from depressurising the pipeline to empty could negate or even have the opposite effect.

6.7.1 Mechanical crack arrestors

Mechanical crack arrestors are devices intended to arrest a crack at a specific location. There are a range of options available:

- *Integral crack arrestor.* – This is a length of arrest pipe installed in the pipeline. The length of the pipe needs to be sufficient to cause crack arrest. Integral crack arrestors are suitable for both ductile and brittle propagating cracks.
- *Non-integral crack arrestor.* – These are typically sleeves installed over the pipe. There are a range of products available, from steel sleeves welded to the pipe or clamped to the pipe (and often grouted for corrosion protection), to glass-reinforced composite wraps.

The primary advantage of non-integral crack arrestors is reduced installation time and cost, because they may be installed on a live line. Such crack arrestors can readily be designed to arrest ductile fractures. However, they can only be relied on to arrest *brittle* fractures if they compress the pipe sufficient to reduce the hoop stress below the stress that would propagate a brittle fracture, removing effective strain energy from the system. Testing (both full-scale and model) has shown that mechanical arrestors must be very tight to achieve this [67].

Achieving this will also generally require that the arrester is installed while the pipeline is de-pressured and hence there is no hoop stress. Then, when the arrestor is re-pressurised, the hoop stress can be transferred to the arrester, rather than the pipe.

The downside of a tight-fitting arrestor is that the crack can be caused to propagate circumferentially at the arrestor, and sever the pipe. The thrust forces from the gas release can then cause lengths of pipe to leave the ditch.

The use of non-integral crack arrestors to control brittle fracture propagation should be accompanied by careful investigation to ensure that suitable testing of the product has been done, and that the installation procedure will reliably result in stress transfer from the pipe to the arrestor, which may be difficult to achieve in practice.

Chapter 7 : Retrospective Fracture Control

The mandatory fracture control requirements for Australian pipelines have changed significantly over the past two decades, reflecting a global improvement in understanding of pipeline fracture, improved testing capabilities, and in some cases increased or decreased conservatism.

Consequently, few older pipelines would satisfy the current fracture control requirements of the Australian Standard (AS/NZS 2885.1). This generally means that such pipelines do not achieve what is now considered best-practice design. In some cases, it also means that there are risks associated with pipeline failure that were not anticipated by the original designers, which have implications for safety management of the pipeline.

The worst outcome for a pipeline failure would be a running fracture that does not arrest. Where the conditions allow for such a failure to occur, the resulting fracture can be kilometres long; the worst known failure occurred in New Mexico in 1960 and extended for around 13 kilometres. For most pipelines, this consequence is catastrophic from both a safety and a supply perspective, and the risk of this is unacceptable.

With the potential consequences being so high, it is important that any existing pipelines should not be carrying unidentified risk associated with pipeline fracture. Consequently, retrospective fracture control requirements have been introduced into AS 2885.3, which specify that assessment is required for pipelines that were designed prior to 2007, or that are being subject to a design-life extension review.

The prescribed method for this retrospective assessment is as follows:

1) A fracture control plan is prepared in accordance with the current requirements of AS/NZS 2885.1.

2) A gap analysis is conducted to determine any non-compliance of the existing pipeline against the current requirements.

3) Identified compliance gaps are subject to risk assessment, as per the methodology in AS/NZS 2885.6.

4) Actions from the risk assessment are implemented, to reduce risks to ALARP.

There are two types of "gaps" that may emerge from the review in Step 2. The first are technical gaps, where the pipe material does not meet the requirements of the latest revision of the standard. The second are information gaps, where there is insufficient data to determine whether the pipe does or does not satisfy the requirements.

Note that the existence of a technical gap does not automatically imply that a pipeline is exposed to unacceptable risk. The requirements of design standards are generally tighter than what is later considered acceptable under fitness-for-service standards. Hence an assessment is required to assess whether the non-conformance to *design* requirements actually results in the pipeline being unfit.

This section defines the retrospective fracture control assessment process in detail and provides guidance for following it. It includes a list of potential technical and information gaps that may exist, and provides guidance for determining the impact that they have on risk. It further addresses strategies that may be used to "close" the gaps, if it is reasonably practicable to do so.

A retrospective fracture assessment will result in development of a fracture control report (FCP) and a safety management study (SMS) update, likely including a separate formal ALARP assessment report. There are a number of ways that the assessment could be documented; it is recommended that the fracture control plan and SMS be updated as distinct documents—the first containing all technical data and assumptions required to define the pipeline's failure modes, and the second containing all threat and risk assessments required to demonstrate ALARP, and details of controls applied.

7.1 Retrospective fracture control methodology

Two retrospective fracture control assessment levels are defined: an initial assessment, and an iterative assessment.

7.1.1 Initial assessment methodology

An initial assessment may be applied to determine the risk ranking. This analysis will be sufficient where the non-compliances result in an acceptable risk for the pipeline—which is likely if the consequences of non-compliance are not significant.

The assessment consists of defining the non-conformance, and demonstrating that the risk associated is low or negligible (both are acceptable levels of risk).

7.1.2 Iterative methodology

An iterative assessment is required where a pipeline is found to be carrying unacceptable risk. The iterations are required to reduce risk by either increasing the rigour of the assessment (refining assumptions), or applying threat controls. The assessment is not complete until the risk does not exceed Intermediate, and ALARP is demonstrated. This means that any additional controls are *demonstrably impracticable*.

Guidance on formal demonstration of ALARP is provided in AS/NZS 2885.6 Appendix I, which is an informative (non-mandatory) appendix. It is recommended that this be reviewed before conducting the ALARP assessment.

Initially, the assessment will be based on a conservative understanding of the threats to the pipeline and a conservative approximation of the fracture properties. If the risk is Extreme, High or Intermediate (but not ALARP), then in each iteration:

- Conservatism can be reduced, by gaining improved understanding of fracture properties, either by reducing safety factors where they are above what is necessary, eliminating assumptions, or obtaining additional data;
- The threat definition may be refined, by obtaining additional data about threats, or subdividing the pipeline into smaller sections with unique threat profiles;
- Risk treatment can be introduced, by controlling the threat, and/or controlling fracture; or
- As a last resort, consideration can be given to replacement, looping or abandonment of all or part of the pipeline.

A detailed outline of the retrospective fracture control process is provided in Figure 7-1.

7.2 Gap analysis

The first step of retrospective fracture control is completing a gap analysis against current requirements. Non-conformances can come in a variety of forms, which relate to the performance inequalities defined in the previous sections:

1) Insufficient ductile fracture arrest control ($FAL > FAL_d$)

2) Too high fracture propagation transition temperature ($FPTT > T_{min,p}$), as inferred from DWTT (T_w)

3) Too high fracture initiation transition temperature ($FITT > T_{min,i}$)

4) Too short critical defect length ($CDL < CDL_d$) for the threat profile

5) Too high energy release rate ($Q > Q_d$)

6) Toughness below the minimum threshold toughness ($C_v < 27$ J)

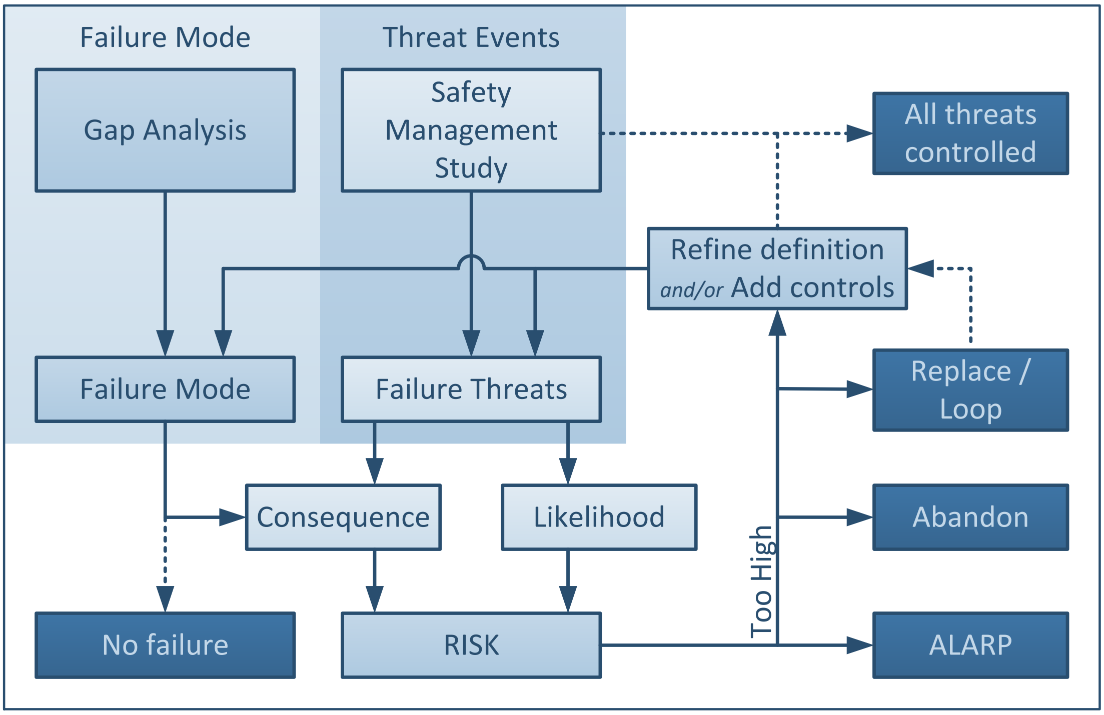

Figure 7-1: Schematic of the iterative retrospective fracture control process.

The margin between the actual and required value for each of these variables will determine the severity of the non-compliance. These potential non-compliances are elaborated on in this Section.

In some locations, there is no mandatory requirement for critical defect length (CDL) or energy release rate (Q), and consequently there is no "non-compliance". However, even where these are not mandated, they still affect the pipeline safety management study because they define the failure mode of the pipeline. After determining the CDL and Q of a legacy pipeline, the existing safety management study assumptions should be reviewed for consistency with them, even if there is no specific non-compliance.

7.2.1 Gap 1 – Insufficient ductile fracture arrest control.

> ***There is insufficient toughness for ductile fracture arrest in all pipe, or in some pipe, so that the fracture arrest length is longer than the specified maximum.***

This is one of the worst potential non-conformances for gas and HVPL pipelines and has varying degrees of severity. If a rupture occurs, the physical extent of the affected area is larger when the fracture propagates.

Where arrest is not predicted in the required one or five pipes (depending on location class) Charpy toughness data, if available, may be assessed to estimate *actual* FAL.

If *some* pipes are arrest pipes, then the consequence is that the FAL is longer than the design FAL_d but is still finite in length. In the worst scenario, *none* of the pipes will be arrest pipes, meaning a rupture may progress uncontrolled ("unzip" the pipeline) over a distance only limited by the length of pipeline between crack arrestors. Such a rupture could potentially be initiated in a rural area and extend into a residential area.

As the FAL increases, the *safety* consequence increases above that of a localised rupture (that arrests in the initiating pipe), though it will still depend on the location and any features that exist within the consequence zone:

- For an unignited rupture, the damaging effect of the explosive release of pressure will spread further. This includes physical damage to infrastructure (such as pavement being torn up) and significant risk to people present when it happens.
- The longer the FAL is, the greater the likelihood of ignition, because the flammable gas cloud covers a greater area.
- An ignited release will have the same release rate (Q) irrespective of the FAL. If the FAL is still small (around 5 pipe lengths or less) relative to the energy radiation contour, the consequence after ignition remains similar to a rupture that arrests in the initiating pipe.
- If the FAL is significantly large, then the two arrest sites will have separate consequence zones, each with a horizontal release, which would potentially be a jet-fire. Refer Figure 7-2.

The *supply* consequence also increases when a fracture propagates. The greater the FAL is, the longer a repair will take to effect, and the greater the amount of spare pipe that will be required to complete it. For pipelines that are critical infrastructure—that is, their continued operation is of critical importance to the community—this may create a higher consequence category than safety considerations for some pipelines.

The cost of the incident also increases. The safety management process in AS/NZS 2885.1 is not intended to consider cost, because it is concerned with public safety. A pipeline owner, however, will need to understand the potential financial consequences of a larger repair when assessing options in an ALARP assessment.

7.2.2 Gap 2 – Too high fracture propagation transition temperature

The fracture propagation transition temperature is above the minimum operating temperature.

When the pipeline steel temperature cools below the fracture propagation transition temperature, propagating fracture is not controlled. This has the same consequences as the previous threat, but only applying while the pipeline is cold.

Generally, buried soil temperatures vary on a seasonal basis—more slowly than surface temperatures. In Australia, soil temperatures one metre below ground commonly vary between 10 and 25°C in cooler regions, and 15 to 40°C in warmer regions.

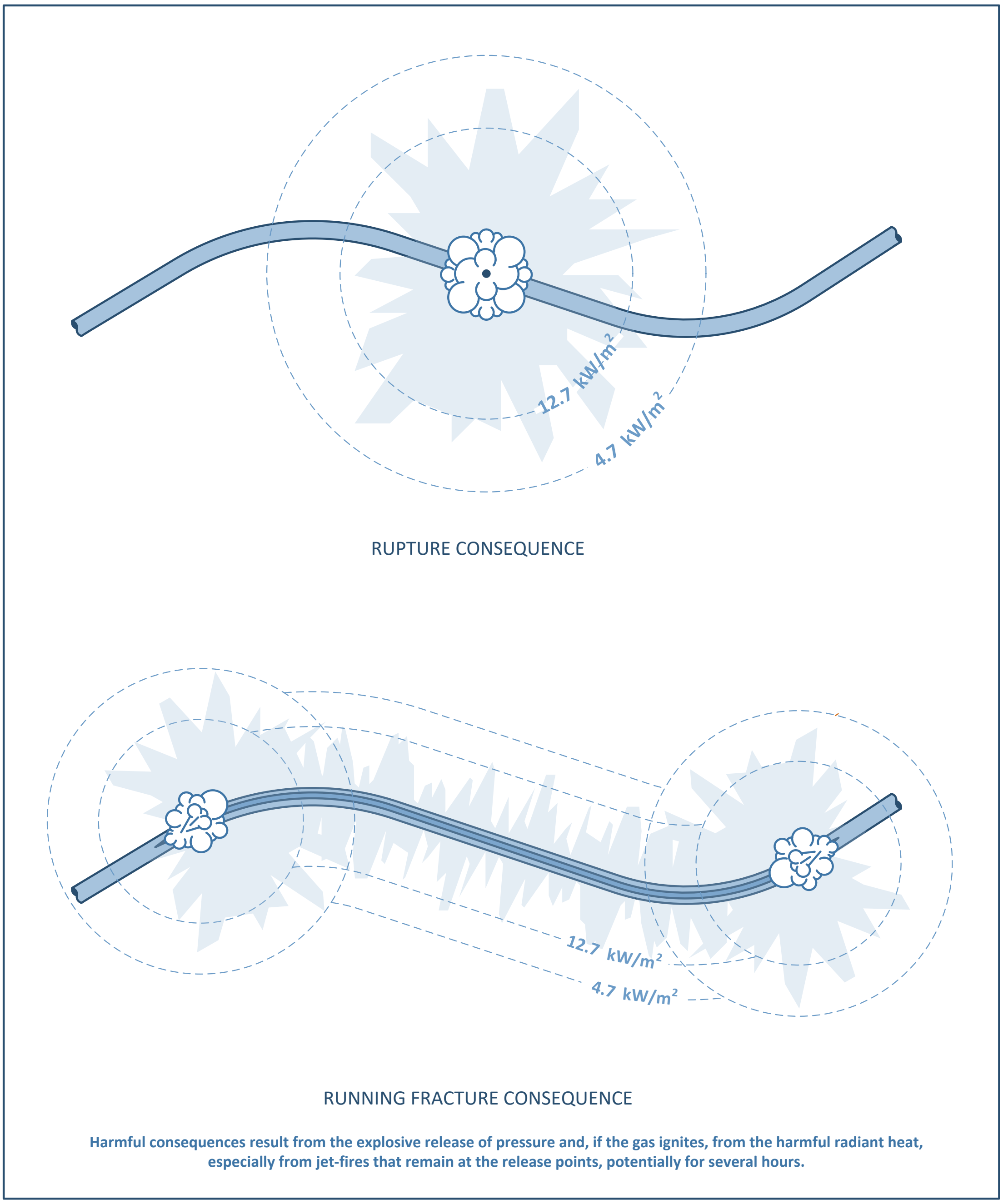

Figure 7-2: The difference in consequence between a rupture and a running fracture.

If the margin between the FPTT and $T_{min,p}$ is small, then this threat may only apply mid-winter, or only apply to shallower portions of the pipeline. The greater the value of the FPTT, the more significant is the risk associated with this non-compliance.

The current requirements of AS/NZS 2885.1 specify that the propagation transition temperature shall be below the minimum temperature for brittle fracture control, T_{BFC}. For retrospective fracture control, however, a pipeline only requires to be fit for service, and the minimum temperature required for propagation control may be taken as the minimum operating temperature. Refer also Section 5.3 for details of low-temperature threats that may apply.

In the worst case, no pipes have a FPTT above the design minimum temperature. The most common response is depressurisation (for instance to reduce stress below 30 %SMYS and the threshold brittle stress) and application of crack arrestors, or abandonment of the pipeline, which will be further elaborated below.

7.2.3 Gap 3 – Too high fracture initiation transition temperature

The fracture initiation transition temperature is above the minimum temperature for initiation control.

Below the fracture initiation transition temperature, the critical defect length (CDL) is significantly reduced, and the pipeline may be vulnerable to failure from super-critical latent defects (e.g. manufacturing defects, SCC, undetected mechanical damage). If fracture initiation does occur, this will result in a leak or full-bore rupture.

The initiation transition temperature will be thirty or more degrees Celsius below the *propagation* transition temperature (see Section 2.6). In practice, this means that it is likely to be below the minimum operating temperature, and the only risk of lower temperature embrittlement results from *transient* low temperature scenarios, as described in Section 5.3.2.

There are two reasons why this is a low-probability problem. Firstly, latent defects are generally part-through-wall defects, and the transition temperature for a part-through-wall defect is generally lower than for a through-wall defect, providing additional safety margin. Because through-wall defects leak, they are usually detected before they fracture, though this depends on the leak rate and the fluid, including, in the case of natural gas, whether it is odorised or not. Secondly, it is known that a defect is less likely to fail if it is loaded *then* cooled down (warm pre-loading), rather than being cooled down and then loaded, which means that introducing cold gas into an already-pressurised pipeline is unlikely to spontaneously cause it to fail [68].

Where this threat is identified, the consequence is a full-bore rupture at a location where the low temperature applies on an existing defect[1] (and possibly a running fracture if propagation is also uncontrolled). The consequences of a full-bore rupture have been described in the previous sections.

7.2.4 Gap 4 – Too short critical defect length

The critical defect length is shorter than would be required for a new pipeline, due to insufficient initiation toughness.

The shorter the critical defect length is, the more likely an external interference threat or a deterioration threat (fatigue, corrosion) will be able to cause a pipeline rupture.

In high consequence areas, a new pipeline is required to have a critical defect length longer than the worst design threat with a safety factor of 1.5. This safety factor accommodates uncertainty.

If a retrospective assessment indicates that the safety factor is less than 1.5, but still greater than 1.0, then this is still an unlikely outcome and might be considered fit for service. If the safety factor is less than 1.0, then the likelihood of a rupture is greatly increased.

A closer assessment can be made to reduce uncertainty in the CDL calculation. This could include use of actual material strength values rather than the specified minimum to calculate the flow stress. This could also involve determining the sensitivity to Charpy toughness and, if the results are toughness-dependent, assessing actual toughness distribution to calculate the *proportion* of pipe with inadequate CDL; this could inform risk determination that uses numerical frequency calculations.

Additionally, a more accurate interpretation of toughness could be gained by some supplementary initiation toughness testing using more precise test methods (see Section 4.2.3), rather than Charpy.

The consequence under consideration from 'Gap 4' is a full-bore rupture, as described above.

7.2.5 Gap 5 – Energy release rate too high

The design external interference threat creates a defect with an energy release rate exceeding what is permitted for the location.

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[1] It is theoretically possible for a new defect to be created during a transient low-temperature event. Generally the likelihood of an initiation event being coincident with a transient low-temperature event is of hypothetical (very low) likelihood.

If an external interference event creates a hole in a gas pipeline, then gas will be released at a rate determined by the hole size. If the leak ignites, then the severity of the consequence will be determined by the size of the radiation zone and what lies in the radiation zone (which is a function of the release rate, Q).

Under AS/NZS 2885.1, high population density areas (T2) are required to have a release rate no higher than 1 GJ/s. In this case, the 12.6 kW/m^2 radiation contour will be 40 m from the release. In residential zones (T1) the release rate is limited to 10 GJ/s, which has a 12.6 KW/m^2 radiation contour radius of 125 m. A non-conforming energy release rate will create an even larger radiation zone.

While these requirements are mandatory, a Safety Management Study will often consider, in relation to specific threat scenarios, the actual consequent release rate, and will review what people or infrastructure may be inside the re-sized 12.6 and 4.7 kW/m^2 radiation zones. In practice, the consequence will be either major or catastrophic (for a release rate of 1 or 10 GJ/s), and a small change in release rate will not change that, so the non-conformance may not be significant.

Note that the defect hole size from a threat is not a function of the critical defect length or any fracture properties. Consideration of energy release rate, however, will be important in conducting a safety management review if there are non-conforming fracture properties.

7.2.6 Gap 6 – Toughness less than minimum required toughness of 27 J

The toughness of steel material – in either line-pipe or a piping component integrally welded to the pipeline – is less than 27 J.

The threshold toughness requirement of AS/NZS 2885.1 is an arbitrary requirement, and so there are no direct consequences from not meeting it. However, the effect of low toughness will impact other gaps above and a very low toughness may invalidate other assumptions made by the standard.

For instance, the threshold stress of 85 MPa is based on the assumption of minimum 27 J toughness, and may not apply if the material is very brittle. Very low toughness will cause the CDL to be very low and may also impact fatigue life assumptions. Finally, 5mm thick material may not be exempt from brittle fracture if the toughness is very low.

7.3 Threat events

7.3.1 Defining threats

The second step of a retrospective fracture assessment is to define the threats to the pipeline, specifically those threats that relate to the non-compliances or "gaps" that triggered the retrospective assessment. There are many threats assessed in a safety management study that will relate to fracture control. These are described in detail in Section 5.2 and 5.3. A selection of common threats are provided in Table 7-1 below; the SMS will provide a full, detailed list.

For a pipeline that already has a Safety Management Study (SMS), it can be the first reference for the threat profile for the pipeline. However, the analysis should not necessarily be limited to threats that have already been identified. The integrity of the overall process depends greatly on the threat identification stage being comprehensive.

The SMS process recommends a control failure check (Section 6.6.1). For pipelines that have inadequate fracture *propagation* control, control failure cases are likely to be the critical consideration. That is, when controls are effective, rupture does not occur. But, if for some reason the controls are ineffective, a propagating crack occurs, which would most likely be a *catastrophic* consequence in a populated area. This is a low-frequency, high-consequence event, and the role of the ensuing ALARP assessment will be to do everything reasonably practical to control the event so that its likelihood is minimised.

Table 7-1 : A selection of common threats that may be relevant for retrospective fracture control assessment

Causes of defects		Causes of Low temperatures
External interference[2]	**Deteriorating defects**	
Excavators	Latent construction defects	Ambient and soil temperatures
Rippers	Stress corrosion cracking (SCC)	Low temperature sources
Vertical boring	Fatigue	Pressurisation
Horizontal drilling	General corrosion	Decompression

[2] This list covers equipment that have the capacity to damage a pipeline. To define the threat however, requires to define *what* the equipment is used for, *who* is using the equipment, *how large* is it and *what* it can do to the pipe in terms of relevant defining parameters (such as teeth type).

7.3.2 Refining definition of threats

After a single iteration of retrospective fracture control assessment, the set of threats to the pipeline may be refined in various ways. The purpose of refining the threats is that the required extent of additional controls may be reduced, which may make the controls cheaper and hence more likely to be reasonably practicable. For instance, installing concrete slabs above a pipeline for its entire length may be prohibitively expensive, but installing concrete slabs in only a few discrete locations may be cost effective.

There are many ways to refine the threats, which include reducing conservatism and gaining data, and subdividing the pipeline to capture differences in threat profile along its length.

Rather than provide an exhaustive list, a range of *examples* of methods for refining the threat definitions have been listed in Table 7-2.

Table 7-2 : Examples of how threat definitions can be refined in an iterative retrospective fracture control assessment.

Gaining data and reducing assumptions.
An initial assessment may include 55-ton excavators, but surveys of landowners and utility companies, or local earthworks contractors and equipment hirers, or a consideration of the geology in the area, could provide a refined understanding of the *actual* size and parameters (e.g. tooth type) of equipment in use and decrease the conservatism of this assumption.
The first assessment may have considered horizontal directional drilling (HDD). A subsequent assessment could carefully consider all the different applications that may use HDD, where it would be used, at what likely depth, and with what drill-bit types, and hence whether (and where) a strike is actually credible.
Initially it may have been assumed that Stress Corrosion Cracking *could* occur. Subsequently an in-line inspection (ILI) using technology suitable for detecting axial cracks (ultrasonic or E-MAT) could be conducted to provide confidence of what the largest defects in the pipeline actually are.
Initially, low temperature modelling may have neglected heat recovery in the soil between two regulators. Subsequent more complex modelling could take into account heat exchange with the soil, or temperature measurement could be conducted, reducing conservatism in estimation of the minimum transient temperature.
The temperature design envelope in the design basis may have been conservative. Review of operating data (where that data spans sufficient time to be representative) may justify changing the design temperature window.

Subdividing the pipeline into areas with distinct threat profiles.
The pipeline may be exposed to the threat of deep ripping, but this threat may only apply over a short part of the pipeline, due to the local land use, specific planned future land use, or only locally having a soil type requiring that treatment (e.g. where the pipeline crosses a rocky plateau).
Low temperatures may result from an upset condition in an upstream facility. The low temperatures may only affect the first few hundred metres of the pipeline, due to heat recovery from the soil. This length could be considered separate to the rest of the pipeline.

These examples demonstrate the way the definition of the threat profile for a pipeline can be refined. After several iterations, the pipeline may be subdivided into a range of sections of which only a few are found to carry unacceptable risk.

7.4 Failure modes

7.4.1 Defining the failure modes

Identifying the technical non-compliance of the pipeline, per Section 7.2, will provide a clear understanding of the failure modes of the pipeline. For any given threat applied at any given temperature, the fracture properties are used to determine whether the pipe is expected to have superficial surface damage, leak, rupture, or have a running fracture (see also Figure 1-1 and Figure 7-3).

The failure mode, in turn, determines the consequence from the threat. The criticality of the pipeline and expected operational downtime will determine the associated *supply* consequence, and the people, infrastructure and any environmental vectors (flora, fauna, waterways etc.) that exist in the vicinity of the pipeline will determine the associated *safety* and *environment* consequences.

7.4.2 Refining failure mode definitions

Just as the definition of the threats to which the pipeline is exposed may be iteratively refined, the understanding of the failure mode and ensuing consequences can also be refined.

If inadequate material data was available for the initial assessment, and assumptions were made about the pipeline's fracture properties, these assumptions may be refined by obtaining material data, justifying less conservative assumptions or applying statistical methods. This can be complicated, and is addressed in Section 7.5.1 below on "Information Gaps".

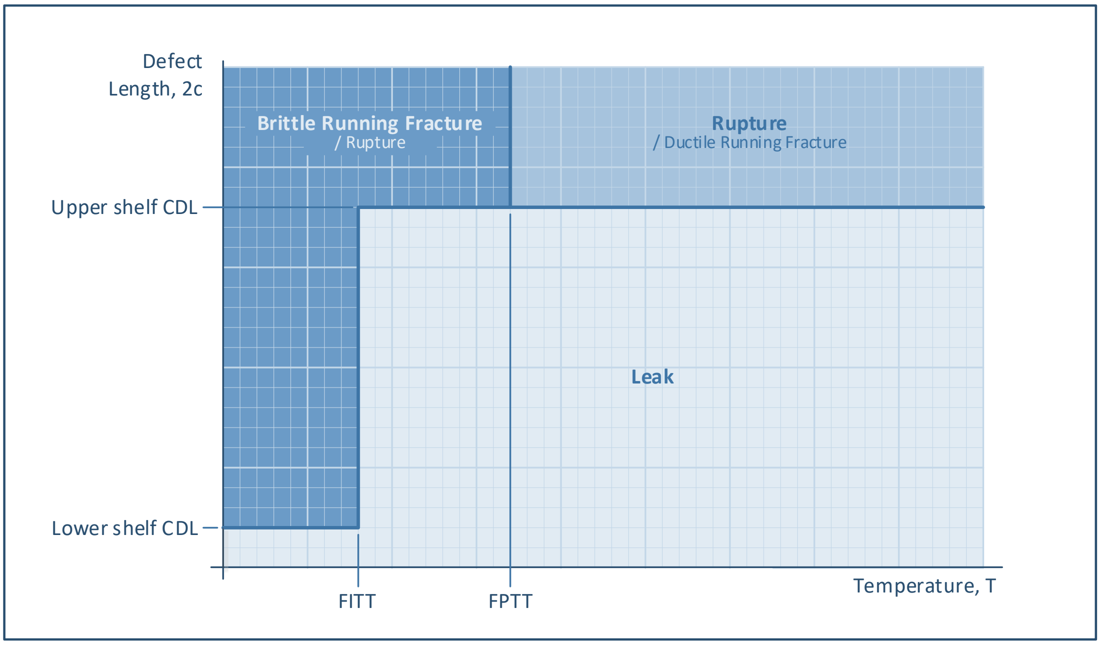

Figure 7-3: Plot of typical pipeline failure modes for through-wall defects.[3]

Alternatively, the pipeline can be subdivided into discrete sections which have different failure mode and/or consequence. Examples of how the pipeline may be subdivided into discrete consequence profiles is provided in Table 7-3.

Table 7-3 : Examples of how failure mode definitions can be refined in an iterative retrospective fracture control assessment.

Gaining data and reducing assumptions
Calculations of critical defect length (CDL) are a function of flow stress and toughness. A superior estimate of flow stress could be made from *actual* tensile test results, rather than specified minimum values. A superior estimate of initiation toughness could be made from actual toughness tests.
Statistical distribution of material properties could be used to estimate statistical distribution of critical defect length. The risk of rupture could take into account the *proportion* of pipe with a CDL below the required value, which could be used in quantitative risk assessment

[3] A pipeline carrying a gas or high vapour-pressure liquid and with insufficient toughness may have "running fracture" in the region designated "rupture". Conversely, a pipeline carrying a liquid or at low pressure may only "rupture" in the region designated "running fracture".

Prediction of penetration resistance most commonly uses conservative guidelines based on typical machinery properties. The methodology in AS/NZS 2885.6 takes into account statistical likelihood of penetration via the "B-factor". The statistical likelihood of penetration could be used directly in quantitative risk assessment. Also, actual penetration field experiments could be conducted to validate calculations and reduce conservatism in the penetration likelihood assessment.
Release rate from full-bore rupture assumes gas flows to the release site from both sides. At the location of a fracture arrest, the gas flows from one side. Release rate calculations can take this into account.
Subdividing the pipeline into areas with distinct consequence profiles.
A portion of the pipeline may have been designated as requiring "no rupture", because it crosses a large industrial area. However, a detailed assessment could identify that the industrial area is largely abandoned or has low population at all times, or there is only one location where there would be significant knock-on effects from a fire, due to nearby bulk flammable chemicals storage (Heavy Industrial location class).
Calculations of CDL and energy release rate (Q) are both a function of pressure, usually calculated at the pipeline's MAOP. A long pipeline may have significant pressure losses, so that high pressures at the downstream end will not commonly occur. At the downstream end, the CDL is longer and the energy release rate is lower than the upstream end. The pipeline could hence be subdivided into sections with different consequence from external interference threats. (To ensure that this is valid, over-pressure protection could be introduced at an MLV, so that even if the pipeline was shut in and the pressure equalised, it would not exceed the new selected analysis pressure).
Initial assessment of the risk of a leak from a liquid pipeline may have assumed that the released liquid will drain into waterways. A subsequent analysis could consider the topography and determine where, exactly, the liquid would be expected to flow. At some locations on the pipeline, the liquid may tend to accumulate in a containable location.
The initial assumption for a liquid pipeline may be that the full inventory leaks. Subsequent consideration of the elevations of the pipeline may identify that at high points, the amount of liquid released is much less.

An initial assumption may assume a fixed probability of ignition for a release (such as 1-in-10 for a leak, and 1-in-3 for a full-bore rupture). Subsequent assessment may more closely consider the potential sources of ignition and determine that ignition is not credible for a range of remote locations or, alternatively, ignition likelihood may be increased at some locations. (Noting that, in the case of external interference, the mechanical equipment that inflicts the damage should also be considered as a potential ignition source).

For some pipelines, heavy-wall pipe in the pipeline (such as used on induction bends, beneath roads, and similar) may be relied on to arrest a crack. The actual locations of heavy-wall pipe can be used to subdivide the pipeline, and this consideration of where the fracture will arrest can define the failure scenarios.

7.5 Controlling threat or failure mode

The third activity relevant to a retrospective fracture control assessment is to control the threats to the pipeline, insofar as reasonably practicable controls can be identified.

Risk analysts use what is known as the hierarchy of controls to define what controls are preferable in reducing risk. There are a few versions of this, for use in different contexts. An approach suitable for pipeline industry is (in order of most to lease effective) elimination, substitution, physical controls, procedural controls, and consequence minimisation—also shown in Figure 7-4.

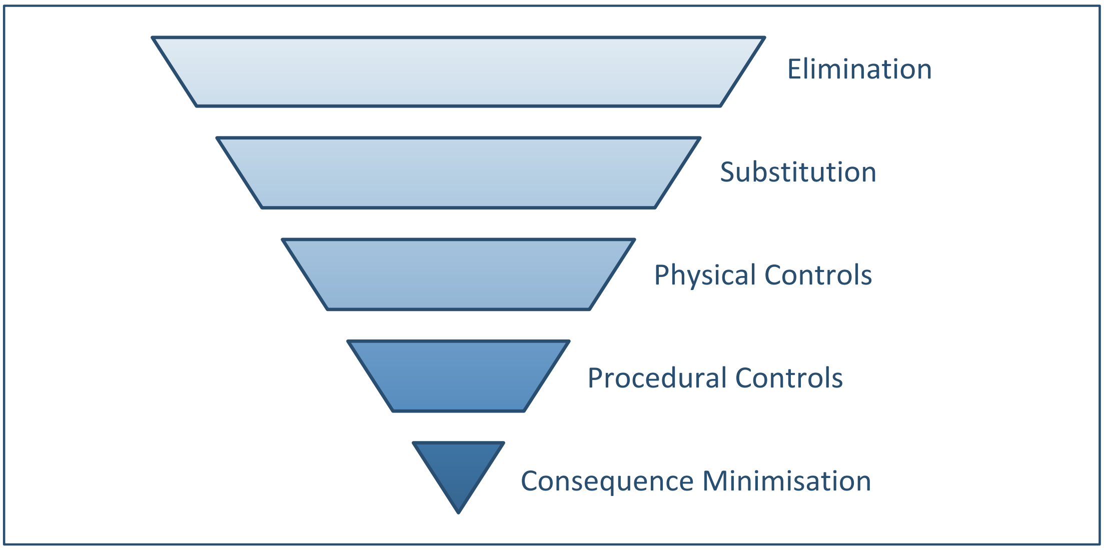

Figure 7-4: Hierarchy of controls.

This hierarchy lists control types in order of effectiveness. However, the most effective control might not be reasonably practicable. (For example, the most effective "elimination" control is to decommission the pipeline, which—though it must be considered in some circumstances—is obviously expensive.) Nevertheless, this hierarchy should be kept in mind when applying additional controls to a pipeline in locations where the risk profile is unacceptable.

Some controls that may be applied are listed in Table 7-4 for consideration, as they may apply to the threats assessed for a pipeline.

Table 7-4 : Examples of controls that may be introduced to eliminate or mitigate threat events, or reduce consequence

Category	Control
Elimination	Purchase and fence the pipeline right-of-way, to prevent any third-party access.
	Separate pressure regulation into several stages with temperature recovery in between.
Substitution	Install low-temperature controls in a facility, so that if a low-temperature upset condition occurs, the cold gas is vented, rather than flowing into the pipeline.
Physical controls[4]	Install concrete or polymer protection slabs, to prevent an excavator striking the pipeline.
	Install fences and crash barriers/bollards at facilities.
	Bury the pipe deeper (lower the pipe), or build up more fill above the pipe.
	Pre-heat gas before pressure regulation to increase the minimum temperature resulting from Joule-Thompson cooling.
Procedural controls	Increase pipeline signage and ensure all signs are legible with the "DANGER" warning.
	Conduct intensified targeted communication to raise awareness of the pipeline.
	Introduce staggered pressurisation procedures that prevent low transient temperatures.

[4] For new pipelines, physical controls include use of thicker, tougher or stronger pipe to increase the CDL.

Category	Control
	Inspect the pipeline using ILI or dig it up and inspect directly. Consider increased inspection frequency for low-toughness pipelines.
Consequence minimisation	Reduce internal pressure, to decrease the energy release rate, and increase the CDL.
	Install main-line valves and leak detection systems, to minimise the inventory available to a leak.
	Install crack-arrestors in strategic locations—typically one measurement-length away from high consequence areas.[5]

7.5.1 Reduction of MOP

Reducing the maximum operating pressure (MOP) of a pipeline to a reduced operating pressure (ROP) is a common control. It will immediately increase the critical effect length and, if the pressure is reduced enough, it can eliminate the risk of fracture initiation altogether. Where relevant, it may also eliminate the risk of fracture propagation.

It is for this reason that the internal pressure of a pipeline is generally reduced by 20% immediately after most pipeline incidents (leak, rupture, mechanical strike and similar)[6]. Any defects that were critical or near-critical at the time of the incident will be sub-critical after the pressure reduction, and hence failure will not be imminent.

Figure 7-5 shows the change in the risk of failure and rupture resulting from an ROP reduction of 20%. This graph was generated for a fictional pipe with 200mm diameter, 5mm wall thickness, flow stress of 360 MPa and high toughness. The through-wall CDL increased from 60 to 80mm. For a defect that is only 60% of the wall thickness deep, the critical length (to fail the defect) increases from about 76mm to over 200mm, significantly reducing the likelihood of a failure.

[5] If a running crack occurs in a gas pipeline, the crack-arrestor will be the location of the long-lasting jet-fire as the pipeline empties. This should be strategically located where the consequence will be minimum.

[6] Note that the pressure should not be reduced to 80% of the *MOP*, but 80% of the *actual recent operating pressure*.

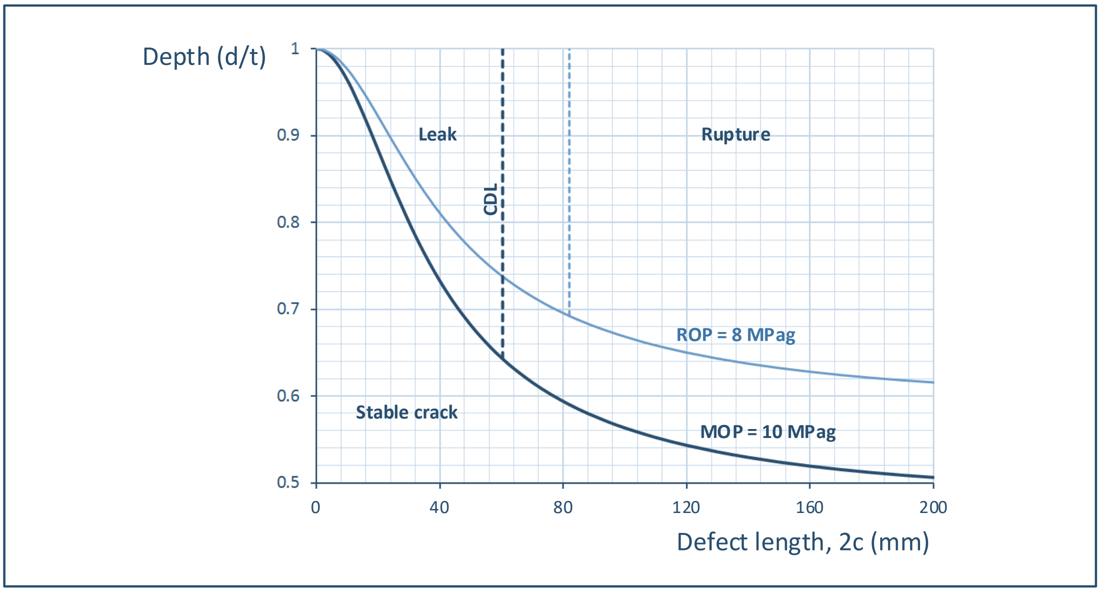

Figure 7-5: The change in CDL for through-wall and part-through-wall defects from a 20% MOP reduction.

7.6 Information gaps

Information gaps apply when there is insufficient data about the pipe properties to confirm whether it does or doesn't satisfy the fracture performance requirements for the pipeline.

Lacking such information will make retrospective fracture control difficult. The following methods could be employed to estimate pipeline fracture properties:

1) Conservatively assume worst-case fracture properties
2) Estimate fracture properties from other known properties and the steel pedigree (steel composition, hardness, strength, manufacturer, year of manufacture and similar)
3) Extrapolate pipeline properties from limited data (potentially obtained from sample testing), with consideration of statistical validity
4) Use a combination of in-line inspection and sample testing to measure and characterise the pipeline properties

More guidance for these is provided in the following sections.

7.6.1 Potential information gaps

Potential information gaps include:

- No Charpy toughness data
- No Drop-Weight Tear Test data

- Inapplicable test data
  - Wrong test piece orientation
  - Wrong test temperature
  - Testing on sheet or plate, before pipe-making
  - No test data available for the seam weld
- Minor non-conformances in the test data
  - Wrong test frequency
  - Testing on flattened specimens
  - No post-coating tests to confirm change in properties from heat-treatment (e.g. on FBE-coated pipe)

Additionally, there may be inadequate information about the physical distribution of the steel heats along the pipeline route. If a pipeline includes more than one production run (continuous production against one specification) or, more importantly, more than one manufacturer, statistics calculations should not consider these as a single population[7]. If the different steels then have significantly different fracture performance, it is important to know where along the pipeline route they have been installed, or to assume that the whole pipeline is from the run with the poorest properties.

7.6.2 Making conservative assumptions

In some cases, it may be reasonable to complete a fracture analysis based on conservative assumptions. Typically, this will only be sufficient if the potential non-conformance is minor.

Making a conservative assumption may require review of known data from similar steels to determine what are the "worst case" fracture properties that could be expected.

Example 1: An ASTM A106 Grade B pipeline has no toughness data. A worst-case assumption may be that it has only 2J toughness at minimum temperatures. Review of supplementary testing on other grade B steels for other projects would likely support that this is a conservative, worst-case, lower-shelf toughness. The pipeline has a low design factor, and 2J turns out to provide sufficient fracture performance for the service.

[7] Within a single production run, it can be assumed that the distribution of properties is Gaussian. Two or more production runs may together have bimodal or other distribution and hence it is difficult to establish confidence intervals for a mixed set like that.

Example 2: Toughness data is available for a pipeline, but there are no post-coating test results, and it is known that the pipeline was heated during coating. The toughness of the bare steel is more than double the required toughness. A review of data from similar steel orders shows that, at worst, the toughness may reduce by 20% after strain-ageing, so the pipe is assumed to have sufficient post-coating toughness to meet the performance requirements.

7.6.3 Estimates from known and measurable properties

Fracture properties are determined from destructive tests. Such tests cannot be conducted on an existing pipeline unless coupons are cut out, which can be expensive and challenging.

One alternative is to collate as much information about the pipe as possible—from any records that are available and through *non-destructive* test methods[8]—and then compare this to other pipes that have similar properties and pedigree. If metallographic grain-size determination demonstrates that the steel is made to fine-grain practice, this alone will permit assumption of baseline toughness, which will be sufficient for some pipe.

Research has been conducted by Pipeline Research Council International (PRCI) to collate a large amount of data on existing pipe and characterise it according to a range of material properties [69]. The analysis included properties that are only obtained by destructive testing, such as toughness, but also data that can be obtained "in the ditch", by digging up the pipe. These included microstructure, composition, and hardness. Use of this research can provide toughness estimates for many existing pipeline steels for which non-destructive data is obtained.

7.6.3.1 Australian legacy steel records

A large portion of the steel in Australia's existing pipelines was manufactured by a limited set of Australian manufacturers, and some of their historical data have been retained.

Such data may be used to establish likely properties for existing pipelines, by the following process:

1) Categorise the pipeline's critical *known* properties: steelmaker, year of manufacture, process of manufacture (or type of pipe), material grade, wall thickness, and diameter.

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[8] Non-destructive testing of hardness and composition are readily available, and some non-destructive tests purport to estimate tensile properties also, though these may have limited reliability.

2) Compile a collection of "similar" pipeline steels, for which test data is available. Existing data will be searchable in the database that APGA is collating.

3) Identify the test results that are relevant to fracture control from the collection of steel data for similar pipelines: Charpy tests, DWTT, yield strength and ultimate tensile strength.

4) Gain a measure of *how* representative are the similar pipe data of the pipeline. Review the dataset for its similarity to the pipeline – i.e. which datasets have the most similar composition, thickness, strength etc. Where they are available in the historical data, variables that can be tested non-destructively on the pipeline are also useful for correlation (such as hardness and microstructure characteristics like grain size).

5) Review the data for correlations in the relevant variables. For instance, if the toughness is found to be correlated with year of manufacture or pipe diameter, then these relationships could improve the estimate for the pipeline in question.

6) Determine the range of probable material properties for the pipeline. If there is a sufficiently large dataset, statistics can be used to define the variables at various confidence levels (P10, P50 and P90 toughness, for instance). Where a relation is observed in step (5), linear regression or curve-fitting can be used. Otherwise a normal distribution can be applied.

In executing this process, consideration must also be given to the stages in the manufacturing process. Data may be available from the slab, from the rolled strip or plate, from the as-welded pipe, and from the as-coated pipe[9]. Two different pipes may be different diameter, but be manufactured from very similar strip product, due to having the same strength and thickness. Identifying that commonality will require some knowledge of the manufacturing journey for pipe.

It is also important to distinguish between the spread of data *within* an order or steel heat, and *between* orders and steel heats.

7.6.4 Sample testing

For many pipelines, some direct physical data will be required, even if only to broadly validate assumptions made using the previous two options.

[9] Generally the strip properties are representative of the welded pipe. The as-coated properties will be modified if the coating process involves heating of the pipe; the effect of that heating will have to be considered as a distinct factor.

There will often be limited material available. Consequently, it is worth carefully planning the number and types of tests, and the test temperatures, to get as much value from the testing as possible. Another way to get maximum value, from Charpy tests in particular, is to report the shear area in addition to the absorbed energy.

Test temperatures should be pragmatic. Using conservative values as may be required for a new pipeline, may misrepresent the actual safety of the pipe. It is reasonable to consult historical operating data for a pipeline to determine what are minimum operating conditions (provided there is a sufficiently long representative history available), and review the design basis if it is found to be over-conservative.

It is also recommended to have a data acquisition plan, whereby every incidental opportunity to collect additional data is taken. For example, coupons can be collected during any hot-tap operation, and non-destructive composition and hardness tests could be completed at any routine verification dig-ups. Over time a library of data about the pipe will be collected, from which conclusions may be drawn with increasing statistical significance.

7.6.4.1 Obtaining pipe samples

Prior to performing retrospective testing, samples must be collected of the correct size and position.

Samples can be retrieved using the following methods:

- existing emergency or spare pipe from the same production run as the pipeline,
- removed pipeline sections from 'cut-out' operations or replacements,
- coupons extracted while the pipeline is decommissioned, and
- hot-tap operations for maintenance purposes, or specifically for the purpose of extracting a coupon from a live pipeline.

If using hot tap coupons, the seam weld's position relative to the coupon should be taken into account. API 5L requires test pieces for the pipe body to be retrieved 90 degrees from the seam weld. However, collection from other areas in the pipe circumference may be more practical and shouldn't produce large deviations in the measured toughness.

When collecting and preparing samples, oxy-cutting should be avoided where possible. If oxy cutting was used, test pieces should be collected outside of the heat affected zone (HAZ) of the cut.

The aim should always be to test the thickest sample possible to obtain toughness as close to the actual pipe toughness as possible. As detailed in Section 4.2.1, if the test sample is too thin, it will provide a more ductile response relative to the test thickness. This must be scaled and is therefore less accurate. From this perspective, gull-wing samples are preferred for thin-wall pipe; however, it may be difficult to find test facilities able to do gull-wing testing.

DWTT samples are much larger than Charpy test samples, so obtaining enough parent metal for testing is difficult unless you have access to whole pipe sections. Obtaining DWTT samples from hot-tap coupons, for instance, would be extremely difficult, especially for pipelines DN300 and below. In this case, Charpy testing could be used to determine the pipes that have the lowest Charpy transition temperature, and these could be targeted for drop-weight tear testing.

7.6.4.2 Statistical analysis

The level of confidence gained from retrospective material testing depends on the number of samples tested. A rule of thumb is commonly appealed to in statistics is that 30 data-points are sufficient to provide confidence (ideally from different heats). However, this depends on the margin of safety between the sample results and the required material properties. (E.g. if a pipeline requires a CVN toughness of 30 J, and there are five results each near 200 J, then that will provide sufficient confidence without needing more data).

It is reasonable to assume that the population distribution of a variable *within a production run* will have a Gaussian distribution. The ‘student t’ distribution can be used to estimate the distribution of the population from the distribution of a sample. More details on how to do this are provided in Appendix C.

Statistics can be used to analyse sample data and estimate both the *mean* and the *minimum* values for the whole production run. In remote areas, where the permissible fracture arrest length (FAL) is five pipe lengths, the *mean* toughness is important, which is relatively easy to estimate using statistics. In a high consequence area, where toughness is required for fracture initiation control to achieve “no rupture” in every pipe, the *minimum* toughness is relevant and is likely more difficult to estimate with confidence. In this case, the retrospective assessment process can be pragmatic, acknowledging that a very small portion of propagate pipes (<15%) will still achieve a high confidence that a crack will propagation at most through one pipe.

7.7 Replace, Loop, Abandon

Fitness for service assessments commonly draw one of the following conclusions, called the four ‘R’s: re-rate, replace, repair or retire. Consideration of replacement, looping or abandonment closes out these potential options for achieving a fit for service pipeline.

Replacing, looping or abandoning a pipeline are last-resort options for a retrospective assessment. These options accept a loss of function for the pipeline because it is carrying unacceptable risk and is no longer fit for either full capacity service (loop), or for any service (replace/abandon).

Looping involves reducing the capacity of a pipeline, and installing an adjacent pipeline to make up the difference in capacity. Replacing is decommissioning the pipeline and installing a new one. Abandonment is decommissioning the pipeline and no longer supplying the transported fluid at all.

The capacity of a pipeline, with regards to either inventory or flowrate, is a function of its pressure rating. The pipeline owner can use the retrospective assessment methodology from this document to determine what is a safe pressure for the pipeline, and consequently review whether the pipeline is able to fill its purpose at that pressure, and what is the best option for the asset and company.

This will be a potentially complicated consideration. If a pipeline requires significant actions to achieve ALARP risk, or the risk cannot be reduced to ALARP, these options need to be considered and the cost can become significant. The process of reducing risk and reviewing ALARP should involve a broad range of stakeholders, including, where applicable, government regulators.

Appendix A Nomenclature

A.1 Symbols and units

The following symbols and acronyms are used throughout this document.

Table 7-5 : Symbols

Symbol	Name	Units
A	Constant in Hollomon equation	Pa
A_c	Pipe internal cross-sectional area	m^2
A_f	Fracture surface area	m^2
A_v	Charpy fracture cross-section area	m^2
$A_{v,FS}$	Full-size Charpy fracture cross-section area	m^2
B	Fluid bulk modulus	Pa
c	Crack half-length	m
c_{eff}	Effective crack half-length	m
C	Constant in fracture velocity equation	$N.m^2.kg^{-1/2}$
C_v	Charpy v-notch test absorbed energy	J
$C_{v,FS}$	Full-size equivalent Charpy v-notch test energy	J
C_w	Drop-weight tear test absorbed energy	J
C_1	Constant (SZMF model)	-
C_2	Constant (SZMF model)	-
CDL	Critical defect length	m
CDL_d	Design critical defect length	m
CDL_h	Limiting high-toughness critical defect length	m
d	Crack depth for part-through-wall defect	m
D	Pipe diameter (outer)	m
D_i	Pipe diameter (inner)	m
D_m	Pipe diameter (mean)	m

Symbol	Name	Units
$D_{m,ref1}$	Reference pipe diameter (SZMF model)	m
$D_{m,ref2}$	Reference pipe diameter (SZMF model)	m
E	Modulus of elasticity	Pa
FAL	Fracture arrest length	m
FAL_d	Design fracture arrest length	m
G	Strain energy release rate	J/m^2
G_C	Critical strain energy release rate	J/m^2
G_D	Dynamic strain energy release rate	J/m^2
G_{el}	Initial elastic strain energy	J/m^2
H	Pipeline burial depth	m
H_0	Reference pipeline burial depth (1m)	m
k	Depth effect exponent in fracture velocity equation	-
K	Stress intensity factor	Pa.m$^{1/2}$
K_I	Mode I stress intensity factor	Pa.m$^{1/2}$
K_{IC}	Critical mode I stress intensity factor	Pa.m$^{1/2}$
K_{ID}	Dynamic mode I stress intensity factor	Pa.m$^{1/2}$
m	Fracture velocity equation exponent	-
M_t	Folias factor, through-wall defect	-
M_p	Folias factor, part-through-wall defect	-
n	Work-hardening exponent	-
N	Number of consecutive propagate pipes	-
p	Proportion of pipes that are propagate pipes	-
P	Pipe internal pressure	Pa
P_a	Arrest internal pressure	Pa
$P_{a,geom}$	Corrected arrest internal pressure (SZMF model)	Pa
$P(x)$	Probability of proposition 'x'	-
Q	Energy release rate	W
Q_d	Design (maximum) energy release rate	W
r	Radius, distance from crack tip (polar coordinates)	m

Symbol	Name	Units
r_p	Plastic zone size	m
R	Pipe radius (mean)	m
R_f	Fracture toughness ratio	-
R_{YT}	Yield to tensile strength ratio	-
R_σ	Stress ratio	-
s	Exponent in SZMF DWTT-teoughness equation	-
S	Proportion shear area	-
S_v	Charpy test proportional shear area	-
S_w	DWTT proportional shear area	-
t	Pipe wall thickness	m
t_{ref1}	Reference pipe wall thickness (SZMF model)	m
t_{ref2}	Reference pipe wall thickness (SZMF model)	m
T	Temperature	˚C or K
$T_{50\%}$	Transition temperature, average of upper- and lower-shelf toughness	˚C or K
$T_{85\%}$	Transition temperature, 85% shear area	˚C or K
T_v	Charpy V-notch transition temperature (50%)	˚C or K
$T_{v,FS}$	Charpy transition temperature (50%) for full-size specimen	˚C or K
T_w	Drop-weight tear test fracture appearance transition temperature (85%)	˚C or K
T_{BFC}	Temperature for Brittle Fracture Control from (from AS/NZS 2885.1)	˚C or K
$T_{min,p}$	Minimum temperature for propagation control	˚C or K
$T_{min,i}$	Minimum temperature for initiation control	˚C or K
U	Fluid velocity	m/s
V_a	Acoustic velocity in the fluid	m/s
V_d	Decompression wave velocity	m/s
V_f	Fracture velocity	m/s
$V_{f,geom}$	Corrected fracture velocity (SZMF model)	m/s
V_{pl}	Plastic wave-speed	m/s

Symbol	Name	Units
Π	Potential energy	J
β	EPCRC fracture model plastic term	-
ε	Strain (true)	-
ε^*	Engineering strain	-
ε_t^*	Tensile strain (at onset of necking)	-
ε_u^*	Ultimate strain (at failure)	-
σ	Stress (true)	Pa
σ^*	Engineering stress	Pa
σ_a	Arrest hoop stress	Pa
$\sigma_{a,max}$	Maximum arrest hoop stress	Pa
σ_{BF}	Threshold stress for brittle fracture	Pa
σ_h	Membrane stress in the hoop direction	Pa
σ_u	Ultimate tensile stress (taken as SMTS for design)	Pa
σ_{yy}	Stress in the y-direction	Pa
σ_Y	Yield stress (taken as SMYS for design)	Pa
σ_f	Flow stress	Pa
θ	Angular position from crack plane (polar coordinates)	°
ρ	Fluid Density	kg/m^2
ν	Poisson ratio	-
δ	Crack-Tip Opening Displacement	m

A.2 Acronyms

Table 7-6 : Acronyms

Acronym	Meaning
ALARP	As Low As Reasonably Practicable
APGA	Australian Pipelines and Gas Association
ASME	American Society of Mechanical Engineers
ASTM	American Society for Testing and Materials
BS	British Standard

Acronym	Meaning
BTCM	Battelle Two-Curve Method
CDL	Critical Defect Length
CDM	Continuum Damage Mechanics
CMOD	Crack Mouth Opening Displacement
CSA	Canadian Standards Association
CT	Compact Tension
CTOA	Crack-Tip Opening Angle
CTOD	Crack-Tip Opening Displacement
CVN	Charpy V-notch
CWP	Curved Wide Plate
DF	Design Factor
DN	Nominal Diameter
DBTT	Ductile-Brittle Transition Temperature
DWTT	Drop-Weight Tear Test
ECA	Engineering Critical Assessment
EI	External Interference
EMAT	Electro Magnetic Accoustic Transducer
EOS	Equation of State
EPCRC	Energy Pipelines Cooperative Research Centre
EPFM	Elastic-Plastic Fracture Mechanics
ERW	Electric Resistance Welded
FAL	Fracture Arrest Length
FATT	Fracture Appearance Transition Temperature
FAQ	Frequently Asked Question
FCP	Fracture Control Plan
FEA	Finite Element Analysis
FFCRC	Future Fuels Cooperative Research Centre
FITT	Fracture Initiation Transition Temperature
FPTT	Fracture Propagation Transition Temperature
GERG	Groupe European de Recherches Gazières

Acronym	Meaning
HAZ	Heat Affected Zone
HFW	High Frequency Welded
HVPL	High Vapour Pressure Liquid
ISO	International Standards Organisation
JSME	Japan Society of Mechanical Engineers
KAPA	Keifner & Associates Pipe Assessment
ksi	One thousand psi
LEFM	Linear Elastic Fracture Mechanics
LMIE	Liquid Metal Induced Embrittlement
LNG	Liquefied Natural Gas
LTS	Low Temperature Separator
MAOP	Maximum Allowable Operating Pressure
MMC	Modified Mohr-Coulomb
MT	Middle Tension
PRCI	Pipeline Research Council International
SAW	Submerged Arc Welded
SCC	Stress Corrosion Cracking
SENB	Single Edge Notched Bend
SENT	Single Edge Notched Tension
SMS	Safety Management Study
SMTS	Specified Minimum Tensile Stress
SMYS	Specified Minimum Yield Stress
SSY	Small-Scale Yielding
SZMF	Salzgitter Mannesmann Forschung GmbH
TMCP	Thermo-Mechanically Controlled Processed

Appendix B Design examples

A DN400 natural gas pipeline is being designed with an MAOP of 10,200 kPag. The pipeline has no corrosion allowance, and will be made from ERW pipe. A simple gas composition has been selected for this worked example:

Element	Proportion (wt%)
Methane	90%
Ethane	5%
Propane	5%

Two examples will be developed for this pipeline, meeting the requirements of AS/NZS 2885.1. The first design is for remote locations, and the second is for residential areas, classified as "high consequence areas".

B.1 Example 1 – Remote locations

In remote locations, there are no mandatory requirements for the critical defect length or the energy release rate. Any fracture that propagates must arrest in five pipe lengths. It has been determined that the lowest operating temperature for the buried pipeline in these areas matches the minimum soil temperature at 10°C. Some of the pipe may see transient temperatures down to 0°C. The fracture control objectives are summarised as follows:

Variable	Value
Q_d	N/A
CDL_d	N/A
FAL_d	5 pipe lengths
T_{min}	10 °C
$T_{min,i}$	0°C

The pipe will be designed with the maximum allowed design factor, 0.8. Several material options will be compared for the calculation: X42, X52, X70 and X80. The worked equations will apply to the X70 option only.

Wall thickness for pressure containment

The minimum wall thickness is given by the Barlow formula:

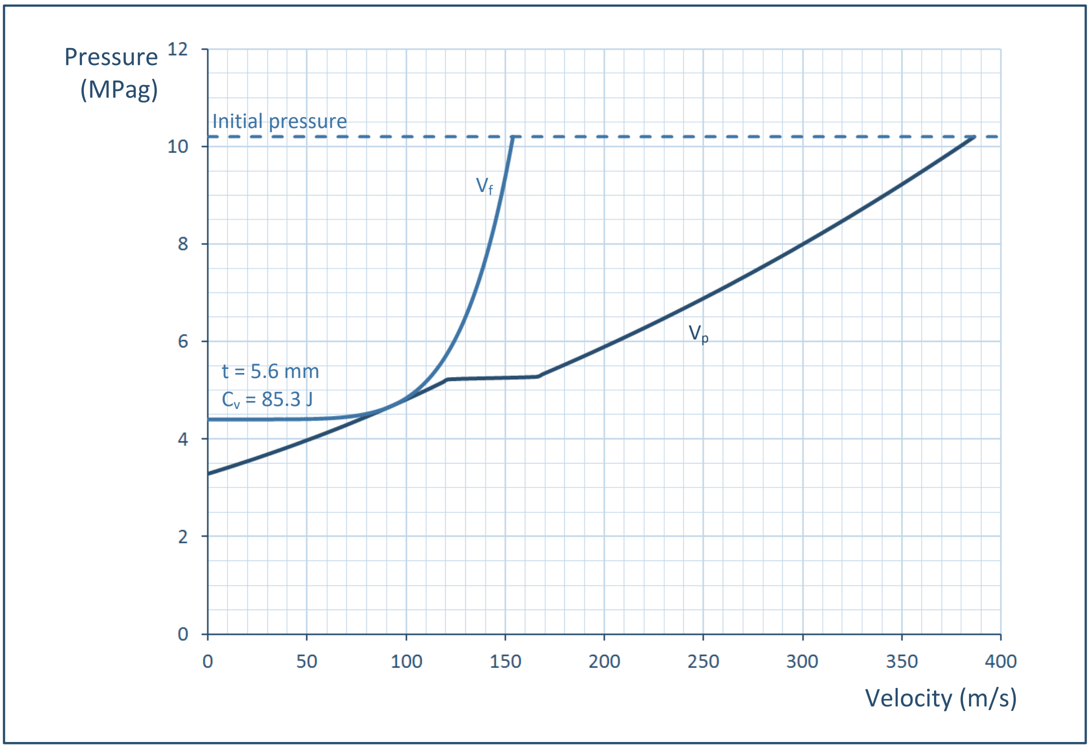
$$t = \frac{PD}{2D_f\sigma_Y} = \frac{(10.2)(406.4)}{2(0.8)(485)} = 5.3\ mm$$

The calculated thickness is then rounded up to the next standard thickness available for API 5L pipe.

Material	σ_Y (MPa)	Required thickness (mm)	Standard thickness (mm)
X42	290	8.9	9.5
X52	360	7.2	7.9
X70	485	5.3	5.6
X80	555	4.7	4.8

Arrest toughness

The arrest toughness was calculated using EPDECOM, with a pressure step of 0.05 MPa, elasticity of 206 GPa, and at the minimum temperature of 10°C. The analysis used the standard wall thickness calculated above.



Battelle Two-curve Model results for the 5.6 mm wall thickness, calculated using EPDECOM.

The calculated arrest toughness for each of the material grade options is provided below:

Material	Nominal thickness (mm)	Maximum arrest toughness (J)
X42	9.5	30.9
X52	7.9	42.7
X70	5.6	85.3
X80	4.8	120.1 Corrected[1] : 168.1

Conclusion

If X70 is selected for the design, the material specification will require:

- Pipe : API 5L X70, DN400, 5.6mm WT
- Average Charpy toughness : Minimum 90 J at 0 °C
- Drop weight tear testing : Shear area > 85% at 0 °C

API 5L and AS/NZS 2885.1 will also require a minimum toughness of 27 J, but the more critical requirement will be from applying a 'statistical factor' of 0.75, creating the following limit—

- Minimum Charpy toughness : Minimum 67.5 J at 0 °C

Under the recommendations in this document (Section 6.2.3), DWTT results could be achieved at 10 °C rather than 0°C and still provide good fit-for-service design.

B.2 Example 2 – High consequence area

The pipeline is being installed in a residential high consequence area, where there is a credible threat of excavators up to 25 tonnes with single point penetration teeth. The pipeline is required to either resist penetration of this equipment, or have a critical defect length exceeding 150mm (1.5 x 100mm, the assumed rupture length for 25t excavators).

This end of the pipeline is adjacent the filling source. During commissioning, transient low temperatures down to –20°C may occur from Joule-Thompson cooling.

[1] According to the rules of AS/NZS 2885.1, the arrest toughness for the X80 option requires to be multiplied by 1.4. This result also exceeds 150 J, and so AS/NZS 2885.1 would require experimental validation from new or existing data to support the toughness specification.

The design requirements are summarised as follows:

Variable	Value
Q_d	10 GJ/s
CDL_d	150 mm
FAL_d	1 pipe length
T_{min}	10 °C
$T_{min,i}$	–20°C

It is likely that this pipe material will also be used for above-ground pipeline assemblies and vehicle crossings, so a design factor of 0.67 has been selected.

Energy release rate

Analysis shows that the gas has a density, ρ, of 0.7674 kg/Sm^3 and a gross heating value, GHV, of 41.94 MJ/Sm^3.

The hole size resulting from the largest equipment (25t equipment with single point penetration tooth) is 65mm diameter. This results in a release rate of 2.035 GJ/s, as per the following calculation (using AS/NZS 2885.6 Appendix B).

Hole area, A:

$$A = \pi\frac{d^2}{4} = \pi\frac{(65)^2}{4} = 3{,}318\, mm^2$$

Mass flow-rate, m':

$$m' = 0.0011pA = 0.0011(10.2)(3318) = 37\, kg/s$$

Energy release rate, Q:

$$\frac{m'GHV}{\rho} = \frac{(37)(41.94)}{(0.7674)} = 2{,}035\, MJ/s$$

Because the release rate is less than 10 GJ/s, it is not a firm requirement to prevent penetration on this pipeline.

Wall thickness for pressure containment

The minimum wall thickness is given by the Barlow formula:

$$t = \frac{PD}{2D_f\sigma_Y} = \frac{(10.2)(406.4)}{2(0.67)(485)} = 6.4\, mm$$

The results for other material grades, and results rounded up to the next standard thickness are as follows:

Material	Required thickness (mm)	Standard thickness (mm)
X42	10.7	11.1
X52	8.6	8.7
X70	6.4	6.4
X80	5.6	5.6

Resistance to penetration

The minimum wall thickness to resist penetration of the largest threat (25 t excavator) is calculated from the method in AS/NZS 2885.1 Appendix E.

The excavator force, F_{bucket}, is calculated for a 25t excavator:

$$F_{bucket} = 7.5W_{OP} - 0.045(W_{OP})^2 = 7.5(25) - 0.045(25)^2 = 159.4\,kN$$

The force required to penetrate the pipe, R_p, is calculated from the tooth dimensions (length, L = 11 mm, width, W = 17 mm) as a function of the thickness, t:

$$R_p = 0.0007t(\sigma_U + 410)(L + 22.4)\left(\frac{W}{W + 3.14}\right)$$

$$R_p = 0.0007t(570 + 410)(11 + 22.4)\left(\frac{17}{17 + 3.14}\right) = 19.34t$$

The penetration conditions are calculated by comparing the two forces. In this equation, the B-factor is taken as 1.3, as recommended in high consequence areas where no-penetration is being used to meet no-rupture requirements.

$$R_p > BF_{bucket} \Rightarrow (19.34t) > (1.3)(159.4)$$

$$t > \frac{(1.3)(159.4)}{(19.34)} = 10.8\,mm$$

The results for other grades are summarised in the following table.

Material	σ_U (MPa)	Required wall thickness (mm)	Standard thickness (mm)
X42	415	12.8	14.3
X52	460	12.1	12.7
X70	570	10.8	11.1
X80	625	10.2	10.3

High toughness critical defect length (CDL_h) calculation

The required critical defect length (CDL) is required to be at least 150mm (if resistance to penetration is possible), hence c = 75.

For this example, the hoop stress will be calculated using the conservative Barlow formula with the outside diameter:

$$\sigma_h = \frac{pD}{2t} = \frac{(10.2)(406.4)}{2t} = \frac{2073}{t}$$

The Folias factor will be calculated per the following formula, as a function of the wall thickness, t:

$$M_T = \left(1 + 2.51\frac{c^2}{Dt} - 0.054\frac{c^4}{D^2t^2}\right)^{0.5} = \left(1 + \frac{2.51(75)^2}{(406.4)t} - 0.054\frac{(75)^4}{(406.4)^2t^2}\right)^{0.5}$$

$$M_T = \left(1 + \frac{34.74}{t} - \frac{10.35}{t^2}\right)^{0.5}$$

The flow stress is calculated by the specified minimum yield stress plus 69 MPa (10 ksi).

$$\sigma_f = \sigma_y + 69 = (485) + 69 = 554\ MPa$$

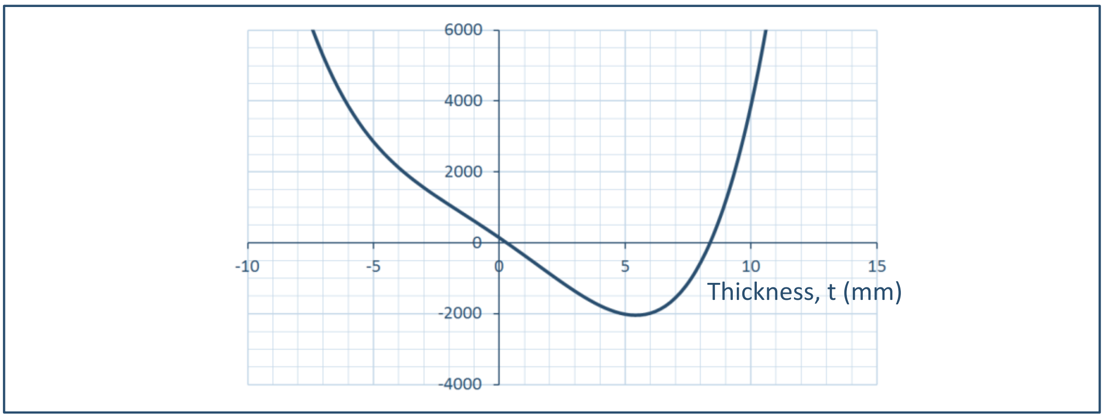
The required thickness was calculated from the following formula, which assumes high toughness conditions.

$$\frac{\sigma_h M_T}{\sigma_f} = 1$$

$$\frac{1}{(554)}\left(\frac{2073}{t}\right)\left(1 + \frac{34.74}{t} - \frac{10.35}{t^2}\right)^{0.5} = 1$$

$$t^4 - 14.00t^2 - 486.3t + 144.8 = 0$$

This quartic is graphed below. The highest positive solution (from Microsoft Excel *GoalSeek*) is the correct answer, at t = 8.369 mm.



Quartic equation solved to determine wall thickness for CDL = 150 mm.

The wall thickness to achieve a CDL_h of 150 mm (assuming high toughness), is provided for all four grades in the following table:

Material	Flow stress (MPa)	Wall thickness for CDL_h = 150mm (mm)	Standard thickness (mm)
X42	359	11.5	11.9
X52	429	10.1	10.3
X70	554	8.4	8.7
X80	624	7.7	7.9

After rounding the wall thickness up to the next standard wall thickness for API 5L pipe, the Folias factor and hoop stress are as follows:

$$M_T = \left(1 + \frac{2.51(75)^2}{(406.4)(8.7)} - 0.054\frac{(75)^4}{(406.4)^2(8.7)^2}\right)^{0.5} = 2.204$$

$$\sigma_h = \frac{pD}{2t} = \frac{(10.2)(406.4)}{2(8.7)} = 238.2\ MPa$$

The above calculations assumed high toughness. Now the *minimum* Charpy toughness required to achieve CDL = 150 mm can be calculated using the following formula:

$$K_{IC}^2 = \frac{8c\sigma_f^2}{\pi} \ln \sec\left(\frac{\pi M_T \sigma_h}{2\sigma_f}\right)$$

$$K_{IC}^2 = \frac{8(75)(554)^2}{\pi} \ln \sec\left(\frac{\pi(2.204)(238.2)}{2(554)}\right) = 1.465 \times 10^8\ MPa^2.mm$$

The toughness is transformed into an energy release rate using the following relations. Note that the units system has to be changed to use Joules.

$$K_I^2 = EG$$

$$G = \frac{K_I^2}{E} = \frac{(1.465 \times 10^8)}{(206000)} = 711.3\ MPa.mm$$

$$G = 0.7113\ J/mm^2$$

In terms of Charpy results, this yields:

$$G = \frac{C_v}{A_v}$$

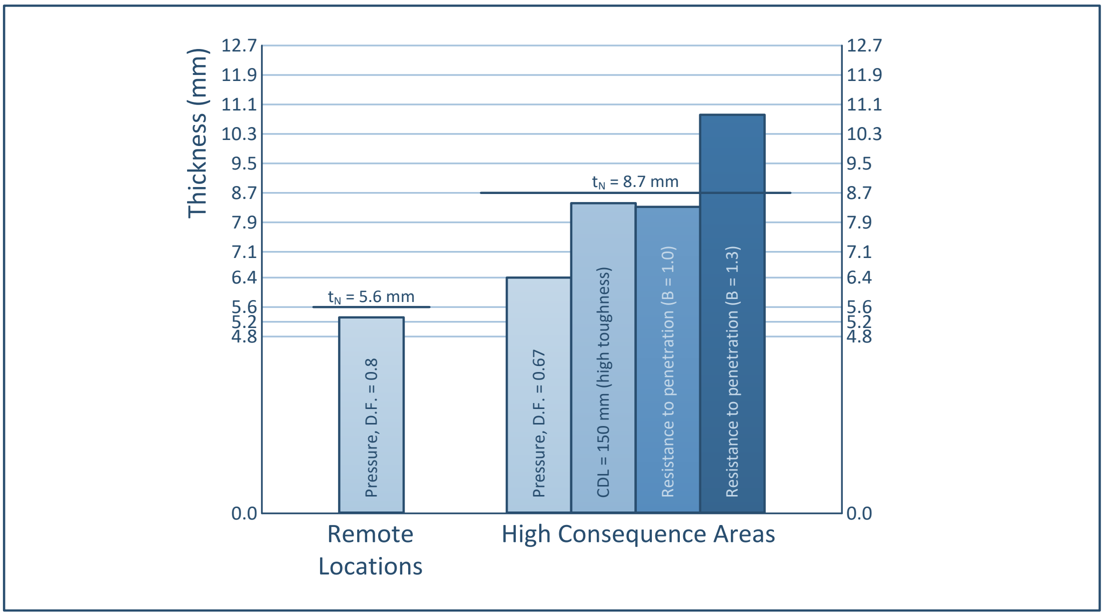
$$C_v = A_v G = (80)(0.7113) = 56.9\ J$$

The results of this calculation for all four grades are summarised below.

Material	Nominal thickness	Toughness required
X42	11.9	25 J
X52	10.3	42 J
X70	8.7	57 J
X80	7.9	82 J

These are all achievable specified toughness values. The X42 grade requires less than 27 J, and so off-the-shelf PSL2 line-pipe would meet this. For the other options, supplementary specification would be required.

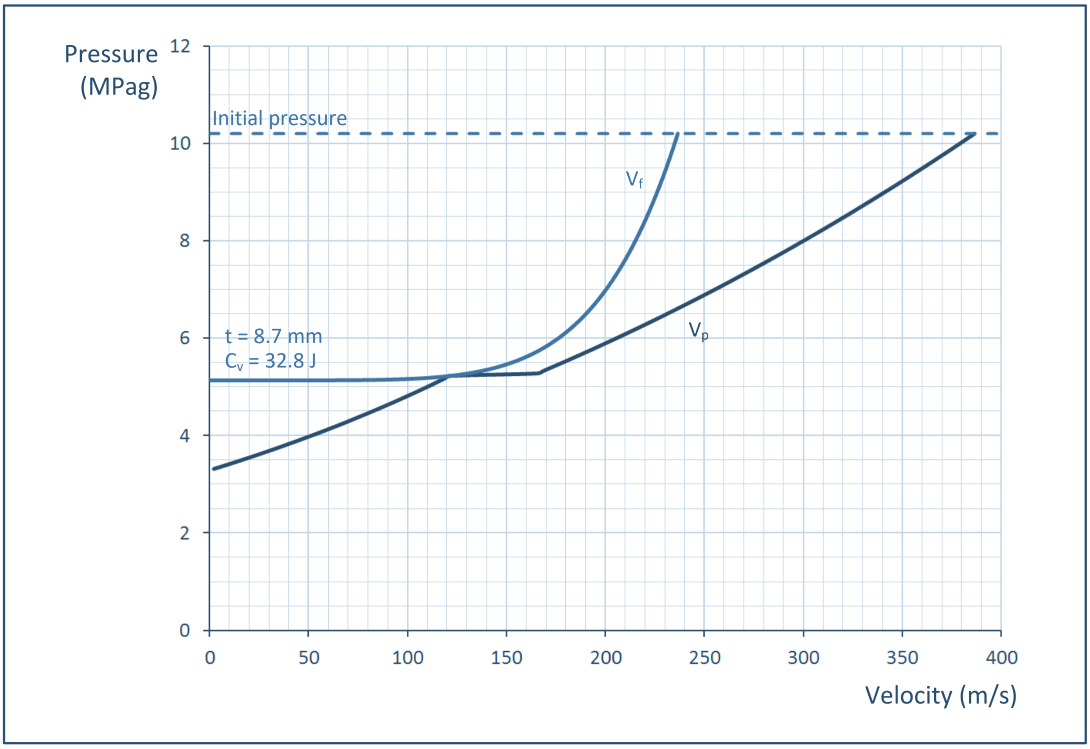
The wall thickness required to keep the CDL less than 150mm is *less* than the wall thickness required to resist penetration. This is due primarily to the B-factor of 1.3. The selected wall thicknesses for achieving the CDL requirement will be used in the rest of the calculation



Summary of wall thickness selection; refer also to AS/NZS 2885.1 Figure 5.2.9.

Arrest toughness

The arrest toughness was calculated using EPDECOM, with a pressure step of 0.05 MPa, and at the minimum operating temperature of 10°C:



Battelle Two-curve Model results for the 8.7 mm wall thickness, calculated using EPDECOM.

The arrest toughness results for the selected wall thickness for other material grades is summarised below:

Material	Nominal thickness (mm)	Maximum arrest toughness (J)
X42	11.9	20.5
X52	10.3	25.8
X70	8.7	32.8
X80	7.9	37.7 Corrected[2] : 52.8

[2] According to the rules of AS/NZS 2885.1, the arrest toughness for the X80 option requires to be multiplied by 1.4, resulting in an arrest toughness of 52.8 J.

Due to the low design factor, these options all result in comparatively low arrest toughness. The toughness demand to achieve "no rupture" requirements is greater (as listed above).

Conclusion

The X70 option, in this case, would require the following specifications:

- Pipe : API 5L X70, DN400, 8.7mm WT
- Minimum Charpy Toughness : 57 J at 0°C
- Minimum *weld* Charpy toughness : 57 J at 0°C
- Drop weight tear testing : Shear area > 85% at –20°C

Note that under the recommendations in this document, the test temperature for the DWTT could be increased to 10°C, and the Charpy testing temperature potentially reduced to -20°C.

B.3 Summary of current AS/NZS 2885.1 requirements

	General	Service	Stress control
Baseline Minimum Toughness	$C_v > 27J$ ...or 40J for transverse specimens	Stable liquid, $T_{min} > 0°C$ An exemption applies to stable liquid pipelines operating above 0°C.	$\sigma < 85$ Mpa, and $D \leq$ DN100. An exemption applies for hoop stress below 85MPa, for diameters less than / equal to DN100.

	Remote locations	Residential, T1	High density, T2
$Q < Q_d$	No requirement In remote locations, there is no specific requirement.	$Q_d = 10$ GJ/s In residential areas, a maximum release rate of 10 GJ/s applies.	$Q_d = 1$ GJ/s In high population density areas, a maximum release rate of 1 GJ/s applies.

	Baseline	High consequence areas	Good practice
$CDL < CDL_d$	$C_V > 27$ J $\Rightarrow$ CDL > 30mm **Except:** Stable liquid > 0°C $D \leq$ DN100, $\sigma_h < 85$MPa	$CDL_d = 1.5$ x defect from design EI threat **Note:** Resistance to penetration calculations use a B-factor of 1.3 for this assessment.	$CDL > 80\%\ CDL_{max}$ Consider any cost-effective ways to increase the CDL, if they provide a meaningful improvement to pipeline safety.

	General	Restraint control	Stress control
$FPTT < T_{BFC}$	$T_W < T_{BFC}$ ...where the FATT (T_w) is determined from Drop-Weight Tear Tests (DWTT)	$t < 5$ mm An exemption applies to any pipe less than 5 mm thick.	$\sigma < 85$ Mpa, and $D <$ DN600. An exemption applies for hoop stress below 85MPa, for diameters less than DN600.

Current fracture control requirements of AS/NZS 2885.1 (2018 revision).

Appendix C Statistical methods

Statistics is used to summarise the variety of data within a dataset. It is important in the pipeline industry, because a pipeline is comprised of a large dataset of individual pipes, each of which has distinct material properties.

Statistics can be especially important when the distribution of known data is used to estimate the probable distribution of unknown data.

Statistics has been discussed in this document in at least three contexts:

- Pipe specification, where statistical variables may be specified (min, max, mean)
- Characterising the effect of heat treatment by testing samples from a pipe order
- Characterising the material properties of an existing pipeline for which a full set of test data is not available (retrospective fracture control)

This appendix provides guidance for conducting these statistics calculations.

C.1 Assumptions made for pipeline material properties

In the pipeline industry, it is common to make the following assumptions:

- The production variables measured for a test unit are assumed to apply to the entire test unit without significant variation. In reality, there will be some variation within a heat and even within a single pipe. The variation *between* test units is more significant than the variation *within* a test unit; however, Example 2 below (C.6) does address measurement inaccuracy for small orders.
- Production variables (Charpy toughness, yield strength, ultimate tensile strength and similar) from a single production run are assumed to have a Gaussian, or Normal, distribution. This is supported by data from real production runs.

C.2 The Gaussian distribution

The Gaussian distribution is a ‘probability density function’ that is shaped like a bell. The curve is centred around the mean, where the probability is highest, and then tapers off on either side. The rate at which it tapers off depends on the standard deviation of the variable.

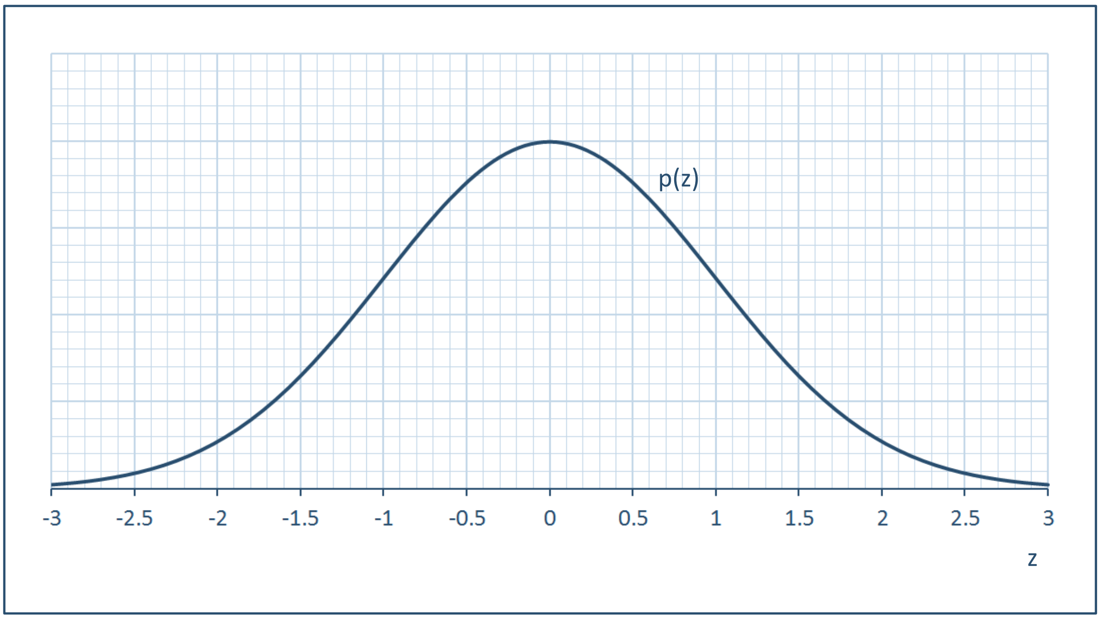
The Gaussian distribution is often normalised by the Z-distribution, which has a mean of 0 and a standard deviation of 1. Any variable, x, can be transformed into a z value using the following formula:

$$z = \frac{x - \mu}{\sigma} \tag{C-1}$$

In this case the probability density function has the following formulae:

$$p(z) = [2\pi . \exp(z^2)]^{-1/2} \tag{C-2}$$

The area under the curve, or "cumulative density" is used to determine the likelihood of results occurring within a range. This integral cannot be evaluated exactly, though approximate formula exist. Many accessible calculators, including free online calculators and *NORM* computer functions in Microsoft Excel, can do the calculation with good accuracy.



Normalised Gaussian probability density function, or "Z-distribution".

C.3 Central limit theorem

An important result in statistics is that the distribution of the means from a set of samples taken from a single population will be increasingly Gaussian as the sample size increases, *irrespective of the distribution of the population itself.*

This theorem is illustrated in the figure below. It can be used to determine the degree of confidence that a sample provides in characterising a population.

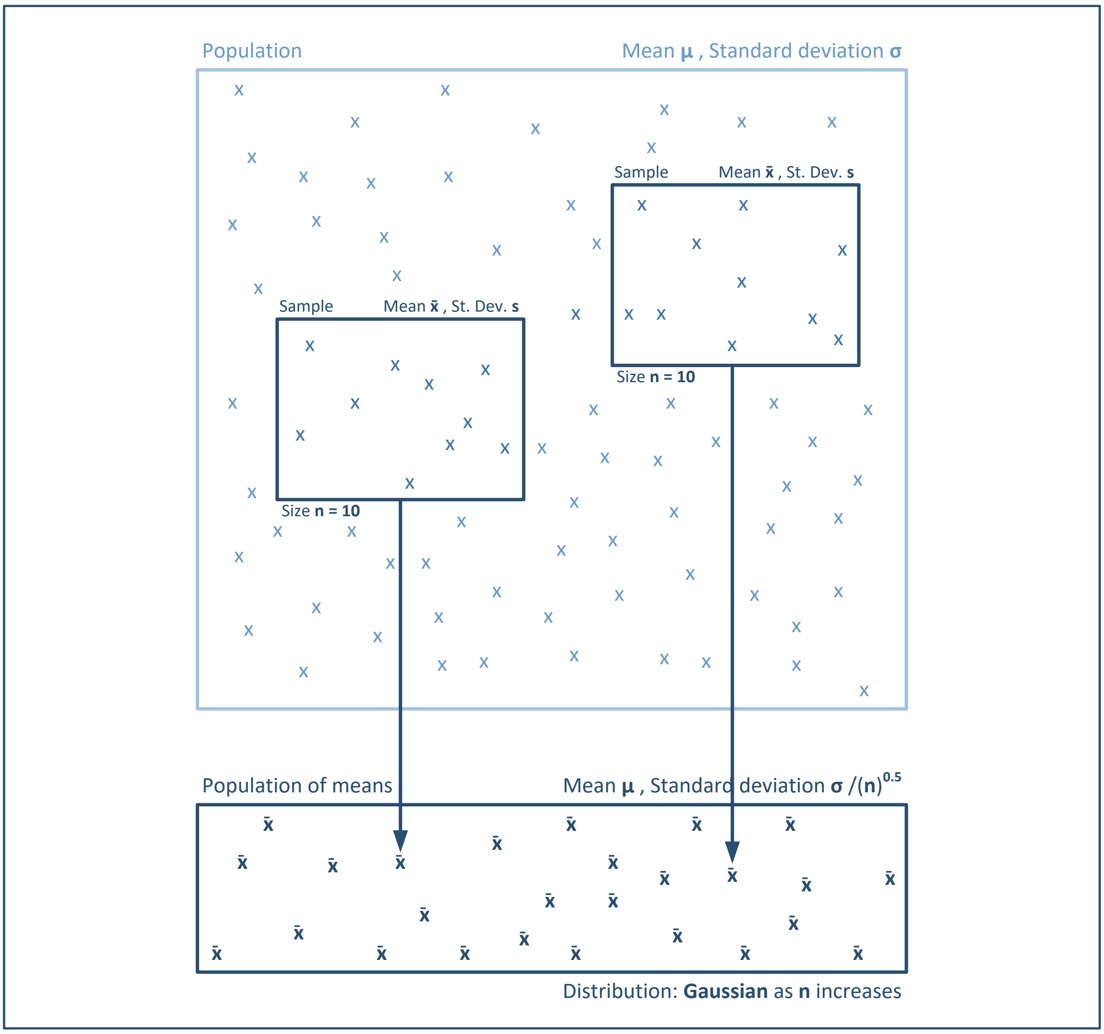


Illustration of central limit theorem.

The mean value of the sample means will be the same as the mean value of population.

The standard deviation of the set of sample means will be the population standard deviation divided by the square root of the sample size:

$$Standard\ deviation\ (\bar{x}) = \frac{\sigma}{\sqrt{n}} \tag{C-3}$$

C.4 T-distribution

The t-distribution is the distribution of a sample mean relative to the true mean. It applies when the population standard deviation is unknown, and so it uses the *sample* standard deviation, s.

The t-distribution itself has a mean of 0 and standard deviation of n / (n – 2) where n is at least 3.

$$t = \frac{\bar{x} - \mu}{s/\sqrt{n}}$$

The probability density function for the t-distribution is more complicated than the z-distribution because it accommodates the uncertainty of the standard deviation. In this equation Γ is the gamma function.

$$p(t) = \frac{1}{\sqrt{\nu\pi}} \frac{\Gamma\left(\frac{\nu+1}{2}\right)}{\Gamma\left(\frac{\nu}{2}\right)} \left(1 + \frac{t^2}{\nu}\right)^{-\frac{\nu+1}{2}} \quad , \qquad \nu = n - 1$$

The variable ν is called the "degrees of freedom" of the distribution.

Free online calculators and the *T* functions in Microsoft Excel can be used to evaluate probabilities from the t-distribution.

The Gaussian distribution may be used instead if analysts have a good estimate for the population standard deviation – i.e. from other material datasets. That would also reduce the conservatism of this method significantly for small sample sizes.

C.5 Example 1 – Estimating the FAL from sample data

Ten Charpy test results are available for a pipeline that was manufactured from a single production run. The pipeline has a calculated arrest toughness of 52 J. The desired fracture arrest length (FAL_d) is 5 pipe lengths or less.

Charpy test results (J)		
66	57	53
85	77	41
60	54	71
	64	

Statistical analysis provides the following:

- Sample mean = 62.8 J
- Sample standard deviation = 12.8 J
- Sample size = 10

Using the t-distribution, the likelihood that the *mean* toughness is greater than 55 J is calculated by the following t-value:

$$t = \frac{62.8 - 52}{\frac{12.8}{\sqrt{10}}} = 2.668$$

Using *Microsoft Excel: P = 1 – T.DIST(1.927, 9, TRUE)*

$$P(t > 2.668) = 1.2\%$$

A lower limit of the mean toughness can be obtained with 95% confidence from *Microsoft Excel: a = T.INV(0.95, 9)*

$$P(t > a) = 0.95 \Rightarrow a = 1.833$$

$$\mu = 62.8 - 1.833\frac{12.8}{\sqrt{10}} = 55.4\,J$$

This analysis indicates that there is greater than 95% probability that the FAL of the pipeline is less than 5 pipe lengths.

C.6 Example 2 – Charpy test results

Charpy tests usually require testing of three specimens. The confidence of the test result relative to the *actual* average toughness of the material can be estimated based on that sample size. The following working assumes that the toughness is homogeneous and spread in Charpy results are caused by measurement error. It also assumes that the population standard deviation is known, and hence uses the z-distribution, not the t-distribution.

The z-value that provides a lower-bound with 95% confidence is calculated from *Microsoft Excel: = 0 – NORM.INV(0.95, 0, 1)*

$$P(z > a) = 0.95 \Rightarrow a = -1.645$$

The standard deviation of the average of the Charpy test results in the sample is related to the sample size and the population standard deviation:

$$\sigma(\bar{x}) = \frac{\sigma}{\sqrt{3}} = \frac{\sigma}{1.732}$$

Consequently, we can say with 95% confidence that the minimum actual average Charpy toughness will be at least:

$$C_{v,actual} = C_{v,meas.} - \frac{1.645}{1.732}\sigma$$

This analysis assumes that the standard deviation of Charpy toughness measurements is known. The more precise the instruments used, the more consistent the Charpy results.

Commonly, the standard deviation could be between 10 and 50% of the average Charpy toughness. In this case, 50% is assumed, indicating:

$$C_{v,actual} = C_{v,meas.} - \frac{1.645}{1.732} 0.5 C_{v,actual}$$

$$C_{v,actual} = C_{v,meas.} \left[\frac{1}{1 + \frac{1.645}{1.732} 0.5} \right] = 0.68 C_{v,meas.}$$

Consequently, in cases where specified minimum toughness is critical to apply to every pipe or mean toughness is required on a pipeline of only one test unit, a safety factor of 1.5 (1/0.68) would provide a suitable level of confidence (unless superior testing precision is demonstrable). Note, however, that these would both be rare, unusual situations; in a high consequence area where no rupture is required, a safety factor of 1.5 is already applied to the critical defect length, and CDL is not strongly toughness-dependent. An alternative to specifying a safety factor of 1.5 would be to increase the sample size (e.g. use an average of six specimen, rather than 3).

Appendix D Fracture control – circumferential defects

Circumferential, or "girth" defects, are controlled very differently to longitudinal defects. While longitudinal defects dominate fracture control considerations in the pipeline industry (due to potential for running fracture), girth defects, especially associated with welds, have driven fracture requirements in station piping codes.

Note that this Code of Practice does not cover longitudinal strain capacity considerations. In some places pipelines are designed to have a specific strain tolerance; it is essential for strain-based design and (commonly required for pipelines in general) that welds will have equal or greater strength than the pipe (called over-matching) so that they do not strain to failure before the pipe has begun to absorb some of the strain. Fracture control is an essential consideration in this strain-based design approach, but will not be covered in this document.

D.1 The difference between girth and longitudinal defects

Longitudinal defects are the emphasis of fracture control of pipelines, which have the potential to become running fractures. The hoop stress loading on a longitudinal defect is usually the highest magnitude stress in the pipe, and consequently when a defect is created from external interference it will almost always propagate longitudinally down the pipe. Under AS/NZS 2885.1, the fracture control plan is exclusively concerned with longitudinal defects.

However, *circumferential defects* are also relevant in pipeline design, and in facility piping design, circumferential defects (especially at welds) are the main consideration in specifying fracture toughness.

Longitudinal defects are proof-tested effectively during hydrotest, because the largest hoop-stress over the life of the facility occurs during the test. Supercritical defects will have to be introduced *after* hydrotest by means of corrosion or fatigue or external interference to cause a longitudinal failure.

Circumferential defects, on the other hand, are loaded by *longitudinal* stress. During hydrotesting, the longitudinal stress is between 0.3 and 0.5 times the hoop stress, depending (respectively) on whether the pipe is axially restrained or unrestrained. During operation, the pipe will also see longitudinal stress due to thermal expansion, and possibly from ground movement and similar effects. The pipe does not necessarily see its largest longitudinal stress during hydrotesting, especially if the design factor is low. Longitudinal welds are *tested* during hydrotesting, but only *proven* for pressure loading, not for other loads that may be applied.

The following table provides a comparison of longitudinal stress resulting from hydrotesting compared to thermal expansion in straight, restrained X52 pipe.

Longitudinal stress from thermal contraction compared to hydrotesting, for an X52 pipeline

Temperature Change	Contraction Stress	Design Factor	Hydrotest Stress Margin[3]
–10 °C	23.4 MPa	0.5	13.5 MPa
–20 °C	46.8 MPa	0.67	18.1 MPa
–25 °C	58.5 MPa	0.72	19.4 MPa
–30 °C	70.2 MPa	0.8	21.6 MPa

This shows that the pipeline cooling by just 10°C at MAOP would cause a greater longitudinal stress than seen during hydrotesting.

If a circumferential defect ruptures the pipe, the result is typically a clean cut dividing the pipe into two. This is a full-bore rupture but, unlike a longitudinal defect, cannot cause a running fracture.

In both facility piping design and pipeline design, circumferential defect failure is prevented by a combination of providing sufficient material toughness and controlling weld defects within a known tolerance.

Although the longitudinal stress can be greater after hydrotest, it is still usually lower than the hoop stress, and the potential size of circumferential defects is limited. Consequently, they are a controlled threat.

D.2 AS/NZS 2885.2 Weld Toughness

Welding of Australian pipelines (both mainline pipe and any "assemblies" designed under AS/NZS 2885.1) is specified in accordance with AS/NZS 2885.2, which sets out three tiers of welding requirements, summarised in the following table.

In each case, testing is required at the lowest design temperature coincident with a combined stress[4] exceeding 30 %SMYS.

[3] This is the difference between longitudinal stress during hydrotest and longitudinal stress during operation at MAOP.

Note that the requirement for pipe body toughness of 27 J specified in AS/NZS 2885.1 is consistent with this requirement, though the exemptions are based only on hoop stress, not combined stress, and the orientation of the specimen is different (longitudinal specimens are used for girth weld toughness).

AS/NZS 2885.2 welding "tiers"

Tier	Requirements
1	**Workmanship standard** Tier 1 welds must meet a set of defined weld defect tolerance requirements. The weld procedure qualification must achieve 27 J Charpy toughness (3 specimen average) in the weld metal, and no less than 21 J for any single specimen.
2 ($t \leq 13$ mm)	**Generalised fitness-for-purpose standard** Tier 2 welds are required to achieve 40 J average Charpy toughness and no less than 30 J for any individual specimen. Tier 2 does not apply for grades stronger than X70.
2 ($t > 13$ mm)	The crack tip opening displacement, δ, shall achieve an average of minimum 0.15 mm and at least 0.10 mm for individual specimens
3	**Engineering critical assessment (ECA)** An ECA is a context-specific analysis of the performance of the weld. It is likely that an ECA will require CTOD testing for more detailed fracture mechanics modelling, with limits determined from job-specific calculations.

Consistent with Section 4.2.3.3, it is recommended to use SENT specimens for Tier 3 analyses. Tier 2 requirements, however, are based on SENB specimens and it would be non-conservative to use a SENT specimen with reference to the 0.1/0.15 mm limits.

Permissible defect size

The permissible defect sizes for Tier 1 are modified from API 1104 to ensure safe operation and are achievable workmanship standards that have a proven history of use in the pipeline industry. In summary, for Tier 1 welds, inadequate penetration, fusion defects, or embedded indications are not permitted to exceed 25mm or 8% of the total weld length (or 50mm and 20% where the wall thickness is greater than 7mm). Undercut of more than 0.8mm depth is not permitted to exceed 50mm length. Cracks are not permitted

[4] Combined stress is defined in AS/NZS 2885.1 as the Tresca or Von-Mises stress that would be in the pipe if there is no defect.

unless they are shallow crater or star cracks less than 4mm in width and length. Full details are provided in AS/NZS 2885.2 Section 17.

The minimum toughness requirement of 27J is an Australian requirement, intended to ensure that these defects will not be supercritical defects in service.

Weld over-matching

Weld “over-matching” describes the situation where, under strain-controlled longitudinal loading, the pipe will yield before the weld does, because the weld is stronger than the pipe. Girth weld overmatching prevents longitudinal strain concentrating at the weld and causing it to fail locally before the strain capacity of the pipe has been exhausted.

Overmatching is specified when either AS/NZS 2885.2 Teir 2 or 3 girth weld acceptance criteria are specified for the pipeline welding, or large strain loads might be applied to the pipeline (such as due to seismic fault movement or land-slip).

Isotropy (refer Section 4.1.3) is commonly assumed for assessment of overmatching. The tensile properties in the longitudinal direction are not normally measured, so it is common to use those measured in the transverse direction. However, the hoop-direction strength may be non-conservative, when the pipe has higher strength in the longitudinal direction, due to rolling effects.

In some contexts, pipelines are designed to have a minimum longitudinal strain capacity. This is called strain-based-design (or limit-state design) and it also is typically applied where large strain loads might be expected. For a strain-based design, the longitudinal strength of both pipe and weld are specified in the design and weld over-matching is critical.

To achieve over-matching, the *net* strength of every cross-section of the weld must exceed the *net* strength of the pipe cross-section. The weld strength may be increased both by increasing the material strength (using stronger weld consumables), or using geometry, by building up a weld cap that makes the weld thicker than the continuing pipe.

Fracture mechanics is relevant to weld matching, because welds are the most likely location for girth defects and, any defects present will reduce the net strength of the weld. Consequently, effectiveness of overmatching must be assessed in conjunction with the permissible weld defect tolerance and weld toughness.

D.3 Facility code toughness requirements

Pipeline facilities are designed to either standard ASME B31.3 or AS 4041. Under AS/NZS 2885.1, pipeline "assemblies" may also be designed to these standards.

In these standards, toughness requirements are a function of material thickness, strength, and post-weld heat treatment (PWHT), which relieves residual stress from welding and reduces load on weld defects. Toughness testing will permit lower operating temperatures, as will decreasing the operating stress.

The derivation of these requirements is based on having sufficient toughness to tolerate permissible weld defects. Welding procedures are designed to ASME PBVC IX for ASME piping. The Australian requirements for pressure-equipment welding are shared between and AS 3992, AS 4458, and AS 4037, the last of which defines permissible defect sizes.

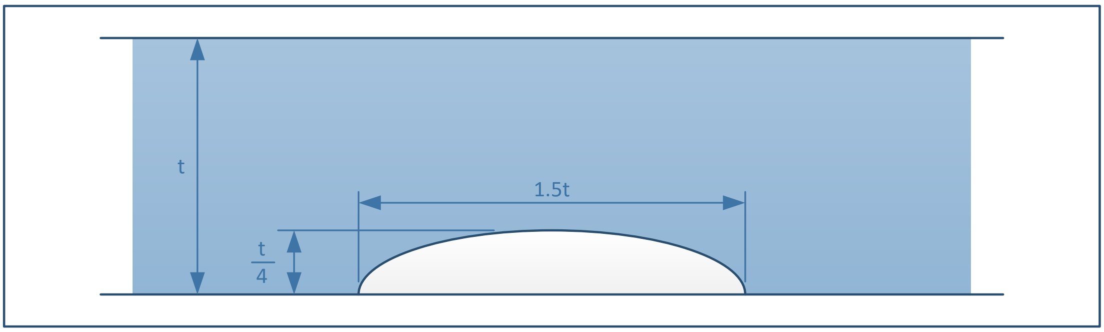
The weld defect tolerances are tighter for facilities than for pipelines, and consequently the toughness requirements are more relaxed in general.

ASME toughness requirements

The ASME standards have a common approach to toughness demand and workmanship, defined in terms of first principles in the ASME Boiler and Pressure Vessel Code VIII division II. This method was revised in the early 2000's and full details of the methodology were recorded in Welding Research Council bulletin 528[5] [70].

The derivation is based on the following assumptions:

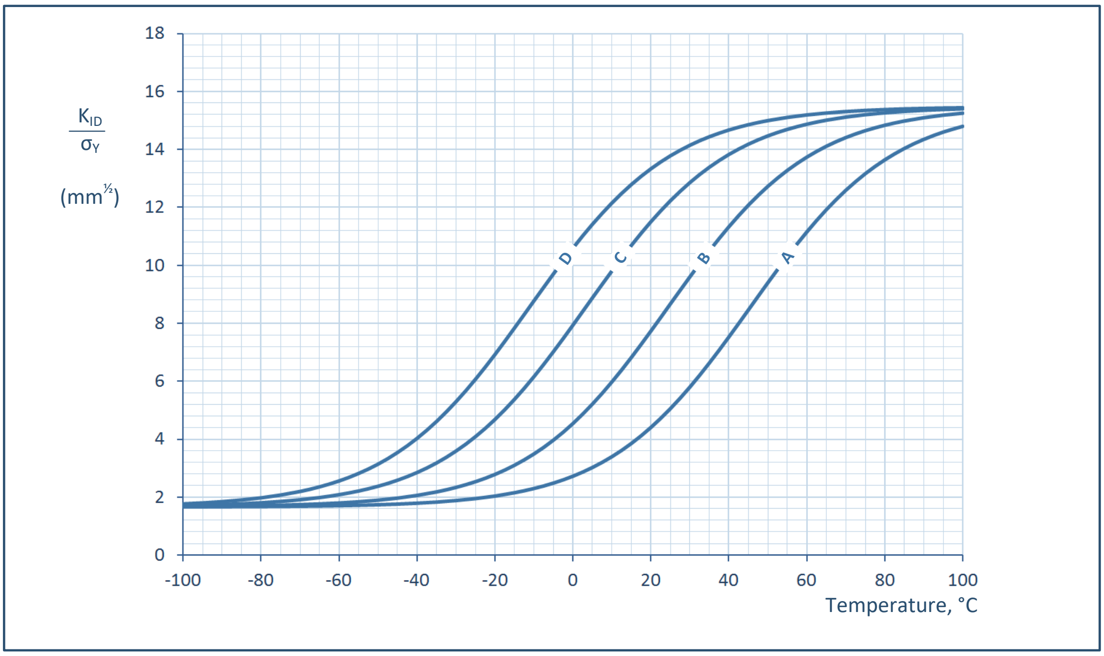
1) The largest defect is a semi-elliptical crack one-quarter of the material thickness deep, and six times that in length.



Assumed defect size used in ASME toughness requirement derivations.

[5] One objective of the revision of ASME BPVC VIII toughness requirements was to align the methodology with API 579, the main fitness for service standard used in both the USA and Australia.

2) Materials are classed into four different toughness specification categories, labelled A, B, C and D, for which minimum specified toughness applies (shown in the following graph).



ASME toughness specification curves. The vertical axis is defined as the critical stress intensity divided by the yield stress, meaning that stronger pipe in each category will also be tougher pipe.

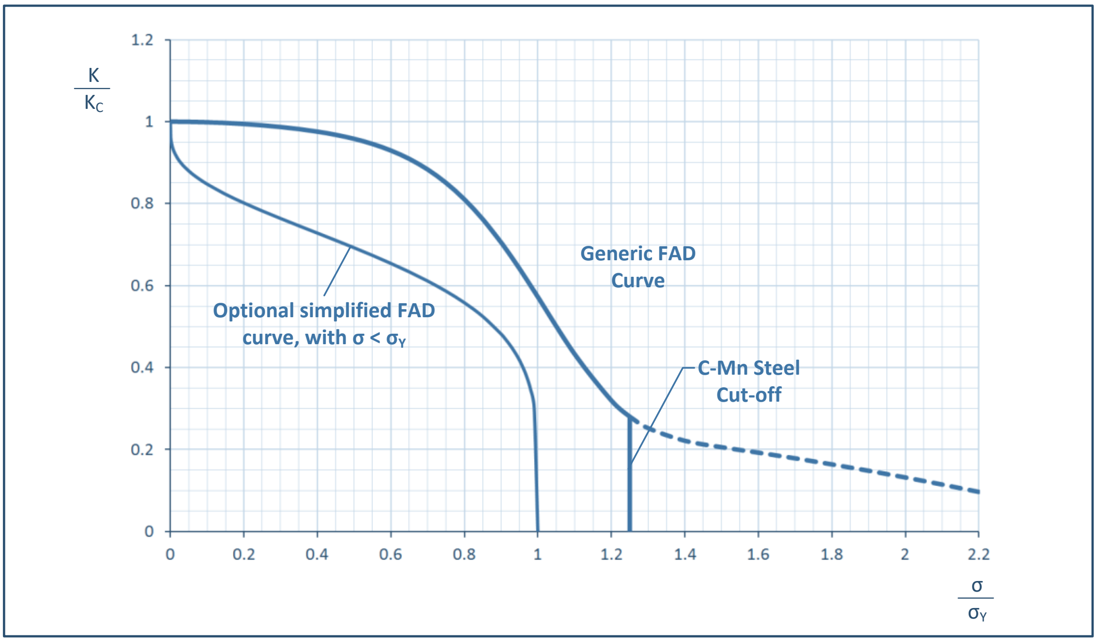
3) The Charpy energy was correlated to critical stress intensity factor, K_{IC}. The lower-shelf energy was correlated according to a form of Equation (2-7), but with a factor of safety of about 3.5 compared to what it would predict. The *upper shelf* full-size Charpy toughness was correlated to the dynamic stress intensity factor according to the Rolfe-Novak-Barsom formula, Equation (4-4).

4) The operating stress was taken to be two-thirds of the yield stress, as per the stress limits from the design standard. The residual stress from welding was also included in the analysis. It was taken to be two-thirds of the yield stress, or 20% of the yield stress in the case of post-weld heat treating.

5) A fracture mechanics assessment was used to determine which materials would require toughness specification, based on their thickness and minimum design material temperature (MDMT). This assessment used the Failure Assessment Diagram method from API 579. This is different to the FAD method from Section 2.4.3 of this report. The FAD from API 579 is illustrated below.

The optional FAD curve, which has the simplified formula from the following equation, was used to develop the ASME fracture toughness curves.

$$\frac{K}{K_C} = \left(1.0 - \left(\frac{\sigma}{\sigma_Y}\right)^{2.5}\right)^{0.2}$$



API 579 failure assessment diagrams, showing the generic curve, the cut-off at 1.25 for C-Mn (Carbon-Manganese) steels, and the simplified curve that can be applied when remaining below the yield stress.

Appendix E Stress state

E.1 The stress tensor

Stress at a point in a material can be represented as a stress “tensor”, which is a 3 x 3 matrix. The columns of this matrix are the three traction vectors described in Section 2.1.

$$\boldsymbol{\sigma} = \begin{bmatrix} \sigma_{xx} & \tau_{yx} & \tau_{zx} \\ \tau_{xy} & \sigma_{yy} & \tau_{zy} \\ \tau_{xz} & \tau_{yz} & \sigma_{zz} \end{bmatrix}$$

The reported stress matrix is a function of the orientation of the coordinate system being used.

Because the matrix is symmetrical ($\tau_{xy} = \tau_{yx}$), the coordinate system can always be rotated to find an orientation that has zero shear stress. In this orientation, the three normal stresses are called “principal” stress, designated (from largest to smallest) σ_1, σ_2 and σ_3, and the stress state is:

$$\boldsymbol{\sigma} = \begin{bmatrix} \sigma_1 & 0 & 0 \\ 0 & \sigma_2 & 0 \\ 0 & 0 & \sigma_3 \end{bmatrix}$$

Though the stress state changes with coordinate system orientation, several matrix “invariants” can be defined. These are scalar values which are always the same, regardless of the orientation of the coordinate system.

The following three standard “principal” matrix invariants have been defined by mathematicians:

$$I_1 = \sigma_1 + \sigma_2 + \sigma_3 = \mathrm{tr}(\boldsymbol{\sigma})$$

$$I_2 = \sigma_1\sigma_2 + \sigma_2\sigma_3 + \sigma_3\sigma_1$$

$$I_3 = \sigma_1\sigma_2\sigma_3 = \det(\boldsymbol{\sigma})$$

Another set of invariants, represented by “J”, are also in use:

$$J_1 = \sigma_1 + \sigma_2 + \sigma_3 (= I_1)$$

$$J_2 = \sigma_1^2 + \sigma_2^2 + \sigma_3^2 (= I_1^2 - 2I_2)$$

$$J_3 = \sigma_1^3 + \sigma_2^3 + \sigma_3^3 (= I_1^3 - 3I_1I_2 - 3I_3)$$

These invariants have been written as functions of the principal stresses. However, their advantage in computation is that they can be calculated from the stress tensor without first calculating the principal stresses.

Any value which is a function of the invariants will also be invariant. Invariants are used in a range of yield and failure models, to define and characterise the stress state. Four examples of this are shown below:

1) Hydrostatic stress

The stress state of a material may be separated into a hydrostatic component and a deviatoric component. The hydrostatic component is an equiaxial stress state, with zero shear in any orientation. The deviatoric component, in contrast, captures all shearing behaviour.

$$\boldsymbol{\sigma} = \sigma_h \boldsymbol{I} + \boldsymbol{\sigma_{dev}}$$

$$\begin{bmatrix} \sigma_1 & 0 & 0 \\ 0 & \sigma_2 & 0 \\ 0 & 0 & \sigma_3 \end{bmatrix} = \begin{bmatrix} \sigma_h & 0 & 0 \\ 0 & \sigma_h & 0 \\ 0 & 0 & \sigma_h \end{bmatrix} + \begin{bmatrix} \sigma_1 - \sigma_h & 0 & 0 \\ 0 & \sigma_2 - \sigma_h & 0 \\ 0 & 0 & \sigma_3 - \sigma_h \end{bmatrix}$$

The "hydrostatic stress", σ_h, is a function of the first stress invariant:

$$\sigma_h = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3} = \frac{I_1}{3}$$

Note that the first invariant of the *deviatoric* stress matrix is zero. In yielding theory, only the deviatoric stress matrix will contribute to plastic flow (yielding). The hydrostatic stress matrix will not change, regardless of the rotation of the coordinate system.

2) Standard invariants for stress analysis

An alternate definition of stress invariants is more commonly used in stress analysis. They are based on the standard invariants defined above, though do not match them specifically. Here they have been distinguished with a prime symbol (') to avoid confusion. These are:

$$J_1' = \frac{1}{3}\mathrm{tr}(\boldsymbol{\sigma}) = \sigma_h$$

$$J_2' = \frac{1}{2}\mathrm{tr}(\boldsymbol{\sigma_{dev}}^2)$$

$$J_3' = \frac{1}{3}\mathrm{tr}(\boldsymbol{\sigma_{dev}}^3) = \det(\boldsymbol{\sigma_{dev}})$$

The first of these invariants is the hydrostatic stress, already mentioned. The second two are functions of the deviatoric stress matrix, and hence relate to the shear stress.

3) Von-Mises stress

The Von-Mises stress, σ_{VM}, is a stress invariant related to the shear distortion energy, and is commonly used to predict yielding. It is determined from the second stress invariant quoted above:

$$\sigma_{VM}{}^2 = 3J_2'$$

This is represented by the following general equation:

$$2\sigma_{VM}{}^2 = \left(\sigma_{xx} - \sigma_{yy}\right)^2 + \left(\sigma_{yy} - \sigma_{zz}\right)^2 + (\sigma_{zz} - \sigma_{xx})^2 + 6\left(\tau_{xy} + \tau_{yz} + \tau_{zx}\right)$$

4) Triaxiality

Stress triaxiality is the ratio of the hydrostatic stress to the Von-Mises stress. It can be expressed as:

$$T = \frac{\sigma_h}{\sigma_{VM}} = \frac{J_1'}{\sqrt{3J_3'}}$$

5) Lode parameter

The Lode parameter relates to the second principal stress, and its magnitude relative to the other two principle stresses. The Lode parameter has been defined as:

$$L = \frac{2\sigma_2 - \sigma_1 - \sigma_3}{\sigma_1 - \sigma_3}$$

This is the ratio of the difference between the second principal stress and the average principle stress, to the maximum shear stress, and can be visualised on a Mohr circle diagram (below).

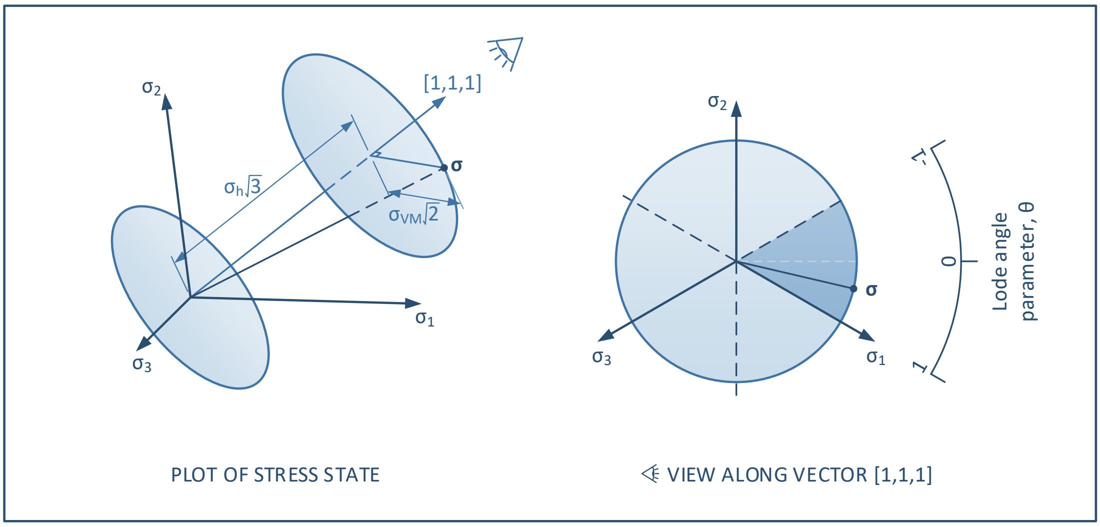
A lode "angle" parameter is defined, which is very similar in magnitude:

$$\theta = 1 - \frac{2}{\pi}\cos^{-1}\left\{\left(\frac{J_3'}{2}\right)\left(\frac{3}{J_2'}\right)^{\frac{3}{2}}\right\} \cong -L$$

E.2 Visualising the stress state

Two common ways of visualising the stress state are described.

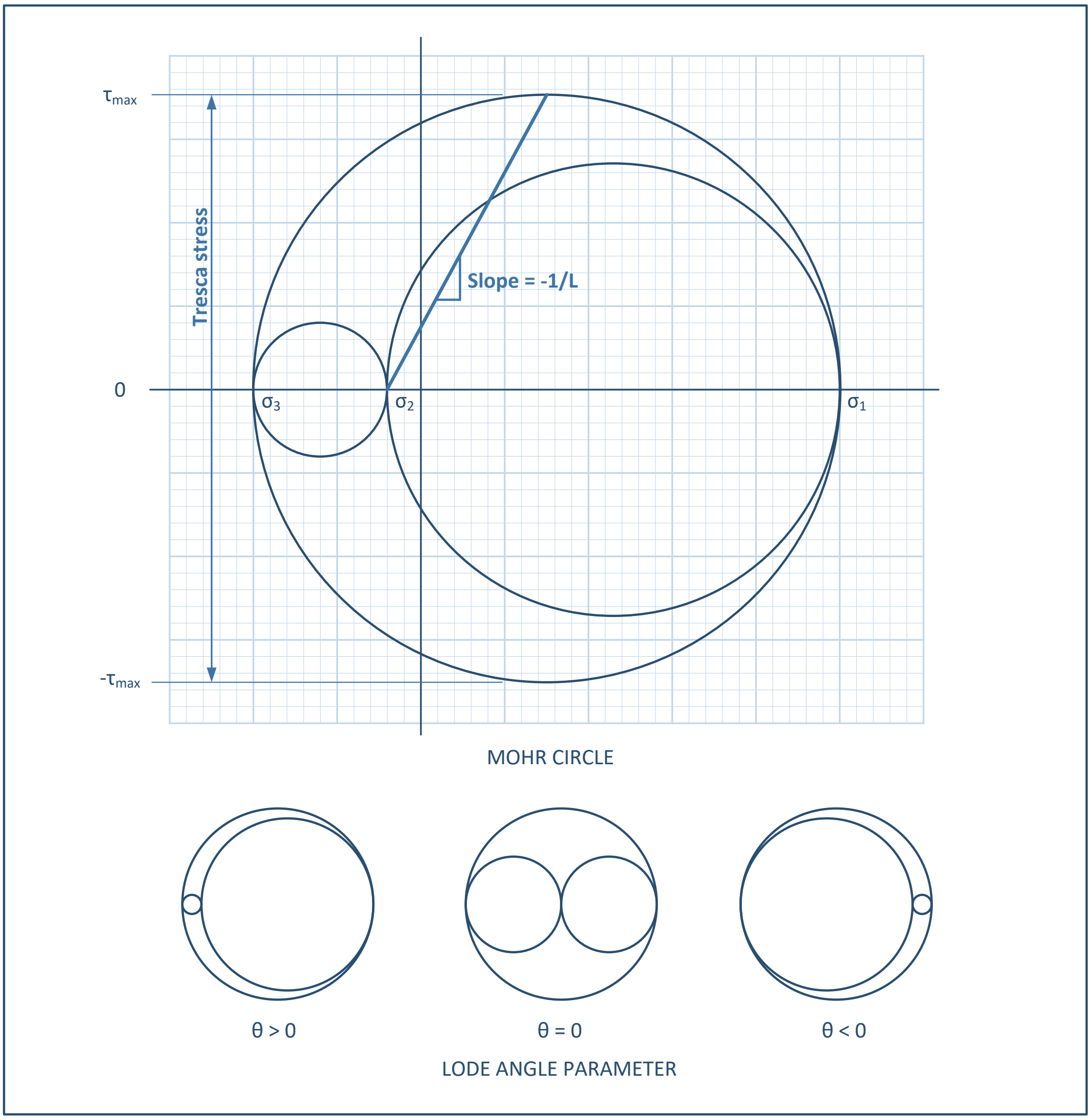
The first is plotting the stress state in 3D space, where the three axes are the three principal stresses.



Visualising stress in the principal-stress space

When viewed along the 1,1,1 vector, the stress state can only lie in a sector of the view, which spans an angle of 60° (because $\sigma_1 > \sigma_2 > \sigma_3$). The position of the stress within that sector is what is represented by the Lode angle parameter.

The second visualisation is the Mohr circle, in which the three principle stresses are placed on a single axis, and the distances between them are used to create circles. The radius of the circles is the magnitude of the shear stress that results if the coordinate system is rotated.



Visualising stress with Mohr Circle diagrams

Appendix F References

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F.2 Standards

Australian / New Zealand standards

AS 1330	Metallic materials—drop weight tear test for ferritic steels
AS 1544.2	Method for impact tests on metals Part 2: Charpy V-notch
AS 1855	Methods for the determination of transverse tensile properties of round steel pipe
AS/NZS 2885.0	Pipelines—Gas and liquid petroleum Part 0: General requirements
AS/NZS 2885.1	Pipelines—Gas and liquid petroleum Part 1: Design and construction
AS/NZS 2885.2	Pipelines—Gas and liquid petroleum Part 2: Welding
AS 2885.3	Pipelines—Gas and liquid petroleum Part 3: Operation and maintenance
AS/NZS 2885.4	Pipelines—Gas and liquid petroleum Part 4: Submarine pipeline systems

AS/NZS 2885.5	Pipelines—Gas and liquid petroleum Part 5: Field pressure testing
AS/NZS 2885.6	Pipelines—Gas and liquid petroleum Part 6: Pipeline safety management
AS/NZS 3788	Pressure equipment—In-service inspection
AS/NZS 3992	Pressure equipment—welding and brazing qualification
AS 4037	Pressure equipment—Examination and testing
AS 4041	Pressure piping
AS 4458	Pressure equipment--Manufacturing

International standards

ASTM A370	Standard test methods and definitions for mechanical testing of steel products
ASME B16.49	Factory-made, wrought steel, buttwelding induction bends for transportation and distribution systems
ASME B31.3	Process piping
ASME B31.4	Pipeline transportation systems for liquids and slurries
ASME B31.8	Gas transmission and distribution piping systems
ASTM E23	Standard test method for notched bar impact testing of metallic materials
ASTM E436	Standard test method for drop-weight tear tests of ferritic steels
ASME BPVC VIII	Boiler and Pressure Vessel Code—Section VIII Rules for construction of pressure vessels
ASME BPVC IX	Boiler and Pressure Vessel Code—Section VIII Qualification standard for welding, brazing and fusing procedures; welders; brazers; and welding brazing and fusing operators
API Specification 5L	Specification for line pipe
API 1104	Welding of pipelines and related facilities
API 579-1/ASME FFS-1	Fitness for service

API RP 5L3	Drop-weight tear tests on line pipe
BS 8571	Method of test for determination of fracture toughness in metallic materials using single edge notched tension (SENT) specimens
CSA Z662	Oil and gas pipeline systems
DNV-OS-F101	Submarine pipeline systems – rules and standards
DNVGL-ST-F101	Submarine pipeline systems
DNVGL-ST-F108	Assessment of flaws in pipeline and riser girth welds
ISO 148	Metallic materials—Charpy pendulum impact test—Part 1: Test method
ISO 15653	Metallic materials—Unified method of test for the determination of quasistatic fracture toughness
ISO 12135	Metallic materials—Unified method of test for the determination of quasistatic fracture toughness of welds
ISO 15590-1	Petroleum and natural gas industries—Induction bends, fittings and flanges for pipeline transportation systems—Part 1: Induction bends