FAILURE BEHAVIOR IN ASTM A106B PIPES
CONTAINING AXIAL THROUGH-WALL FLAWS

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ABSTRACT

The pressure load limit for a cylindrical shell containing a flaw is a function of cylinder radius and wall thickness, flaw dimensions, and the properties of the material. The strength of large diameter thin-wall cylinders of brittle material containing axial through-wall flaws can be predicted by linear elastic fracture mechanics, by applying suitable corrections for the effects of curvature. For cylinders of more ductile material, it is necessary also to take into account the effect of plastic yielding at the tip of the advancing flaw, and workers in the United States and the United Kingdom have developed relations between burst pressure, flaw size, and material properties which predict the behavior of axially flawed cylinders for which the product of radius and wall thickness is 20 in.² or greater.

Results of hydrostatic tests at 60°F on flawed ASTM A106B pipes in the diameter range of 4 to 12 inches are presented. Variation of limit pressure with flaw length and pipe dimensions is described for pipes having radius-wall thickness products as low as 0.5 in.². Development of an empirical equation to predict behavior of small diameter, ductile pipes having axial through-wall flaws is discussed.

INTRODUCTION

For a piping system or other pressure-containing system to be used safely, it is necessary that some knowledge of the maximum pressure load it can support without leakage or catastrophic fracture be available to the designer and user. For geometrically simple structures, such as cylindrical or spherical shells, limiting pressure loads have been calculated and reported in the technical literature.⁽¹⁻³⁾ Although differing in detail, all these calculations are based on some assumed stress-strain relationship for the material and a yield criterion, such as that of Tresca or von Mises-Hencky. In each case, it is assumed that yielding is homogeneous up to the maximum load point, beyond which localized deformation sets in and leads rapidly to failure if the applied load is maintained. In the absence of either strain hardening or favorable stress-strain rate dependence, homogeneous plastic deformation might not be expected to occur at all since any existing geometrical discontinuity, no matter how small, must introduce local stress variations in the structure. Under monotonically increasing tensile load, yielding may be expected to occur first in regions of locally reduced cross section or stress concentration, and thereby accentuate preexisting discontinuities in effective load-bearing cross section. It may be shown⁽⁴⁾ that for a solid cylinder to extend plastically under uniaxial tension without necking it is necessary that

$$\frac{1}{\sigma} \frac{\partial \sigma}{\partial \epsilon} + \frac{\dot{\epsilon}}{\sigma} \frac{\partial \sigma}{\partial \dot{\epsilon}} \geq 1 \quad (1)$$

in which σ is the instantaneous tensile stress, ϵ is the true strain, and $\dot{\epsilon}$ the strain rate. In the absence of dependence of stress on strain rate, the load maximum for a cylinder or other structure of uniform cross section extending under uniaxial tension is given by

$$\frac{d\sigma}{d\epsilon} = \sigma \quad (2)$$

Conditions corresponding to (1) and (2) exist for deformation under multiaxial tensile stresses typical of which is the expansion of a spherical or cylindrical shell under internal fluid pressure. For a material exhibiting Ludwik-type strain hardening* the load maximum under uniaxial tension occurs when the true strain equals the strain-hardening exponent. Plastic extensions of 20 to 40% before the load maximum are typical of many metals.

Satisfaction of condition (1) or its multiaxial counterpart is sufficient to ensure stability against necking and localized failure as a result of normal small surface irregularities, and properly designed and fabricated structures do undergo significant deformation before the load maximum is reached. It is obvious there is a limit to the extent to which strain hardening can compensate for geometrically induced stress inhomogeneity and that a flaw or other geometrical discontinuity above some critical size must initiate localized deformation leading to failure before the normal load limit is reached. The path by which this localized deformation leads to failure depends upon the material and the flaw configuration. In brittle materials, crack propagation leading to failure occurs with plastic deformation limited to a very small zone about the crack tip. In more ductile materials, plastic deformation is also initially limited to a small zone about the flaw tip but it may spread to encompass the entire structure before the ultimate failure load is reached. The behavior of brittle structures containing flaws has been rather successfully treated analytically by linear elastic fracture mechanics. The behavior of ductile structures in which extensive or general plastic yielding precedes failure has not been so treated except in an empirical and rather qualitative way. The following discussion deals with some experiments on the failure behavior of ASTM A106B steel pipes containing axial through-wall flaws. The use of an empirical equation for prediction of the failure pressures for such pipes is also described.

BEHAVIOR OF PIPES WITH AXIAL THROUGH-WALL FLAWS

The failure stress for a wide plate of brittle material containing a through-wall crack and under uniaxial tension normal to the crack plane is given by the relation

$$\pi \sigma_c^2 = K_{Ic}^2 \quad (3)$$

in which σ is the applied stress, c is the crack half-length, and K_{Ic} is a material constant; the so-called plane strain fracture toughness or critical stress intensity factor defined by linear elastic fracture mechanics.⁽⁵⁾ If the material is not completely brittle, Equation (3) must be corrected by a term which is a function of the ratio of K_{Ic} to the yield stress of the material. Dimensionally, the equation is unchanged; i. e., the limiting stress the plate will sustain varies inversely as the square root of the crack length. The fracture mechanics analysis indicates that a tensile stress component parallel to the crack should not affect the value of the instability stress normal to the crack plane; thus a crack in a very large diameter thin-wall cylindrical shell under internal hydrostatic pressure should approach the behavior of the crack in a wide thin plate under uniaxial tension equal to the hoop stress. However, as the diameter of the shell decreases it is necessary to take into account bending stresses about the crack. Anderson,⁽⁶⁾ using dimensional reasoning, developed an expression of the form

$$\sigma^2 \left[\pi c + \frac{1}{2} \left(\frac{K_{Ic}}{\sigma_B} \right)^2 \left(1 + q \frac{c}{R} \right) \right] = K_{Ic}^2 \quad (4)$$

* For such a material $\sigma = \sigma_0 \epsilon^n$ in which σ_0 is a constant and n is known as the strain-hardening exponent.

in which K_c is a nominal stress intensity factor, σ_B is the biaxial yield stress, c is the crack half-length, R is the cylinder radius, σ is the membrane hoop stress at instability, and q is a "bulge coefficient" dependent on material properties and cylinder radius.

Irvine, Quirk, and Bevitt,⁽⁷⁾ using experimental data from tests on 5-ft-diameter pressure vessels, concluded that the load limit for a thin wall cylinder under internal pressure was given by an expression of the form

$$\sigma_c^3 = K \quad (5)$$

in which K is a constant dependent on ultimate strength, yield strength, and the Charpy impact energy of the materials. The form of the relationship between this constant and the material properties of the material was dependent on the fracture mode observed.

Eiber and his co-workers⁽⁸⁾ developed an equation relating burst hoop stress σ_h in pressurized pipes to an effective stress intensity factor K

$$K^2 = \frac{\pi c \sigma_h^2}{\cos \theta} \left(1 + \frac{5\pi}{32} \lambda^2 \right) \left(\frac{4-\kappa}{2} \right) \quad (6)$$

where

K = Stress intensity factor, ksi $\sqrt{\text{in.}}$,

c = Half of the crack length (through-the-wall cracks), inches,

$$\lambda^2 = \frac{c^2}{Rt} \sqrt{12(1-\nu^2)},$$

ν = Poisson's ratio,

R = Radius of vessel, inches,

t = Wall thickness, inches,

$$\theta = \frac{\pi}{2} \frac{\sigma_h}{\sigma_o},$$

σ_h = Nominal hoop stress, ksi,

σ_o = Failure hoop stress for unflawed vessels, ksi,

κ = $(3-4\nu)$ plane strain; $(3-\nu)/(1+\nu)$ plane stress.

This equation, intended to be chiefly applicable to large diameter pipes, has been applied by Eiber to the fracture behavior of axially flawed low carbon steel pipes for which the product of radius and wall thickness is 20 inch² or more. It will be observed that for flaws short in comparison to Rt , Equation (6) reduces to the typical elastic fracture mechanics form

$$\sigma_c^2 = \text{constant} \quad (7)$$

while for long flaws the form

$$\sigma_c^2 c^3 = \text{constant} \quad (8)$$

is approached. The Irvine, Quirk, and Bevitt relation lies between these extremes.

EXPERIMENTAL PROCEDURES

The work to be reported here has been limited to specimens of one material, ASTM A106B pipe in sizes ranging from 4 to 12 inch nominal diameter. Segments of length at least four times the diameter were closed at the ends with standard welding caps of the same material as the pipe. Axial through-wall flaws of varying lengths were cut in the centers of the specimens with a saber saw. The ends of the flaws were sharpened either by hand-broaching to a tip radius of approximately 0.001 inch or by cyclic pressurization of the specimen until visible fatigue crack propagation had occurred. Before welding the end caps on the specimens, each flaw was sealed with a laminated patch consisting of a layer of 1/32-inch-thick annealed aluminum next to the pipe wall and extending approximately 2-1/4 inches beyond the flaw in each direction, a somewhat smaller piece of 1/16-inch-thick annealed mild steel, and finally a piece of 1/16-inch-thick Neoprene extending about 1 inch beyond the edges of the aluminum. The patch was formed to the proper curvature and assembled outside the pipe with General Electric RTV 102 cement between metal layers and RTV 90 cement between the Neoprene and the metal. After curing, the completed patch was cemented in place with RTV 90 cement between the Neoprene sheet and the pipe wall. While the cement was curing, the patch was held in place in the pipe by an inflated rubber bladder and the pipe was warmed slightly with an infrared lamp.

The specimens were tested hydrostatically with water or water-ethylene glycol mixtures as the pressurizing fluid. Hydrostatic pressure was generated with an air-driven piston-type booster pump having a piston area ratio of 300 to 1. An electrical contact built into the end of the driving cylinder closed when the piston was fully retracted. Pressure applied to the specimen was measured by a strain-gage-type pressure transducer connected through an appropriate signal conditioner to a strip-chart potentiometer recorder. The chart paper on the recorder was advanced by a solenoid driven ratchet motor actuated by electrical pulses from the contact on the pump. It was found that the volume of water delivered per stroke of the pump was essentially constant over the pressure range covered in the tests. Since chart motion was proportional to the number of pump strokes, the recorder produced a plot of applied pressure against volume of water delivered to the specimen. If the compressibility of water is neglected, the chart record amounted to a plot of load versus deformation for the specimen. A typical record, exhibiting a distinct yield point, is shown in Figure 1. In early experiments of the test series specimen deformation was measured by post-yield strain gages attached in a circumferential direction to the mid-portion of the specimen approximately 90 degrees from the flaw. After development of the volume recorder, strain gages were not used, and general plastic deformation of the specimen was determined by pre- and post-test measurements of pipe circumference.

MATERIAL

ASTM A106B seamless pipe is a low cost steel of 0.30% nominal carbon content which is neither isotropic nor particularly uniform with respect to mechanical properties. The material used in these tests was intended to be representative of routine manufacturing practice; no effort was made to select it on a basis of special properties. Mechanical tests of the material were limited to tensile tests and Charpy impact fracture tests with either full-size specimens or sub-size specimens when specimen size was limited by pipe wall section thickness. Figure 2 contains data from a typical single length of 6-inch Schedule 80 pipe which are illustrative of the anisotropy of fracture toughness in this material. In Table 1 are given the room temperature tensile properties of the material used, along with the burst hoop stress for unflawed pipe of each size.

FIGURE 1. PRESSURE VERSUS DEFORMATION RECORD
FOR ASTM A106B PIPE, UNFLAWED

FIGURE 2. TOUGHNESS ANISOTROPY OF ASTM A106 PIPE (6 inch Schedule 80)

TABLE 1. Room Temperature Tensile Properties of ASTM A106B Pipe Used in Pressure Load Limit Tests

Size (inch) and Schedule	Specimen Direction	0.2% Yield Stress, ksi	Ultimate Stress, ksi	Percent Elongation	Burst Hoop Stress, * ksi
12-80	C	45.8	68.6	31.0	61.2
10-40	L	38.9	64.3	35.0	-
10-40	C	38.2	65.0	30.4	56.3
10-80	L	38.4	72.2	32.2	-
10-80	C	48.9	73.5	26.5	66.0
8-40	L	48.8	74.6	28.4	-
8-40	C	50.3	76.2	27.9	69.4
8-80	L	41.3	67.3	29.6	-
8-80	C	44.6	68.1	26.0	62.2
6-40	L	41.2	61.1	37.0	-
6-40	C	43.8	62.6	30.4	62.0
6-80(a)	L	39.7	65.5	34.0	-
6-80(a)	C	45.3	66.5	27.4	58.8
6-80(b)	C	50.0	67.3	25.5	65.4
4-160	L	37.4	70.0	33.7	-
4-80	L	44.2	72.8	32.2	-
4-40	L	43.3	68.8	32.7	57.7
3-160	L	38.0	62.9	32.7	-
3-80	L	45.0	64.2	33.4	-
2-160	L	49.4	80.2	27.5	-
2-80	L	52.2	73.5	25.6	-

C = Circumferential
L = Longitudinal

(a) and (b) indicate two distinct
heats in this size

* For an unflawed pipe specimen, based on original dimensions.

RESULTS

Twenty-one specimens of A106B pipe in various diameters, wall thickness, and with different flaw lengths were tested. Typical failure appearances are shown in Figure 3. Maximum hoop stress is plotted against flaw half-length in Figure 4 for six different pipe sizes. General yielding, if the load maximum was not reached first, occurred at hoop stresses equal to approximately 1.15 times the uniaxial tensile yield stress as would be predicted from theory. There was no evidence that the behavior of fatigue sharpened flaws differed from that of the hand-finished flaws. Data points for each size of pipe are seen to be consistently above or below the drawn curve. The relative position of these points with respect to the curve cannot be accounted for by the corresponding heat-to-heat variation in mechanical properties as given in Table 1. If maximum hoop stress is plotted against flaw half-length divided by pipe mean radius, the spread of data points about the drawn curve is reduced (Figure 5). The dotted line in Figure 6 indicates the ASME code allowable stress⁽¹⁰⁾ for ASTM A106B pipe at room temperature. The intersection of this line with the curve indicates that a flaw of length approximately equal to the pipe diameter is needed to reduce the load maximum under monotonic loading to the level of the code allowable stress.

a. 8-inch Schedule 40, 3-inch Flaw
Maximum Hoop Stress 31.8 ksi

c. 6-inch Schedule 80, Unflawed
Maximum Hoop Stress 58.8 ksi

b. 12-inch Schedule 80, 2-inch Flaw
Maximum Hoop Stress 38.4 ksi

d. 6-inch Schedule 80, 8-inch Flaw
Maximum Hoop Stress 10.3 ksi

FIGURE 3. TYPICAL FAILURE BEHAVIOR IN HYDROSTATIC TESTS OF ASTM A106B STEEL PIPE

FIGURE 4. VARIATION OF PRESSURE LOAD LIMIT WITH FLAW LENGTH IN ASTM A106B PIPE WITH AXIAL THROUGH-WALL FLAWS

FIGURE 5. PRESSURE LOAD LIMIT VERSUS c/R FOR ASTM A106B PIPE WITH AXIAL THROUGH-WALL FLAWS

FIGURE 6. EFFECTIVE CRITICAL STRESS INTENSITY FACTOR FOR ASTM A106B PIPES CONTAINING AXIAL THROUGH-WALL FLAWS

Values of effective critical stress intensity factor or fracture toughness were calculated from the load maximum data by using Equation (6). These values are plotted against flaw half-length in the upper part of Figure 6. The increase in K with increasing flaw length is not surprising since the authors of the equation make no claim for its validity at large values of c^2/Rt . By experimentation it was found that an empirical equation of the form

$$K^2 = \frac{\pi \sigma_n^2 c}{\cos \theta} \left(1 + 0.4 \frac{c^2}{Rt} \right) \quad (9)$$

greatly reduced the variation in K with flaw length. Values of K calculated by using Equation (9) are shown in the lower part of Figure 6. Again, there is no obvious correlation between the scatter in the values of K about the mean value of 95.6 ksi√in. and the heat-to-heat variation in either tensile yield stress or the burst hoop stress for pipe in the unflawed condition.

In an effort to resolve this question, a number of half-size (1 × 0.5 cm) Charpy impact specimens were prepared from the different sizes of pipe. In each case, the specimen length was in the circumferential direction, and the notch, of 0.003-inch root radius, was parallel to the pipe radius. Impact tests were made at three temperatures with a low energy (28 ft-lb maximum) test machine giving an impact velocity of 11 ft/sec. Kinetic energy corrections were made

from the measured trajectories of the broken specimens. Results of these tests are given in Table 2. A portion of the specimens were tested as machined. The results of these tests are presented as fracture energy in foot-pounds. These data are not directly comparable to fracture energy data obtained with standard 1×1 cm Charpy impact specimens, but are useful for qualitative heat-to-heat comparisons. The balance of the specimens were surface nitrided by exposure to 35 to 40% dissociated ammonia for 24 hours at 935°F . Fracture energy values from these specimens are presented in terms of fracture energy per unit fracture area (W/A) in inch-pounds per square inch.

TABLE 2. Relative Toughness of ASTM A106B Pipe Material

Pipe size, in.		Test Temperature, $^{\circ}\text{F}$	W/A in-lb/in ²	Estimated K, ksi $\sqrt{\text{in.}}$	Fracture Energy, * ft-lb
Diameter	Schedule				
4	40	33	73	48	5.7
4	40	60	106	58	8.9
4	40	80	137	66	13.9
6	40	33	88	53	3.7
6	40	60	115	61	9.0
6	40	80	192	78	21.9
6	80	33	62	44	3.0
6	80	60	60	44	4.6
6	80	80	66	46	10.1
6	80**	34	81	51	-
6	80**	60.5	111	60	-
6	80**	145	1350	208	-
8	40	33	49	40	8.8
8	40	60	60	44	16.0
8	40	80	77	50	16.4

* Non-nitrided 1×0.5 cm specimens.

** 1×1 cm specimens, nitrided.

It may be safely assumed that the fracture energy per unit fracture area obtained in a properly performed impact fracture test is equal to or greater than the value of G_{Ic} for the material at the same deformation rate. Difference between measured W/A and G_{Ic} is caused by the difference in deformation rate and to the failure to achieve plane strain fracture conditions in the impact test. This error may be greatly reduced if surface shear fracture is suppressed by a brittle nitride case on the surface of the specimen. If one takes all possible steps to obtain plane strain fracture conditions, the relation

$$K_{\text{Ic}} \geq \sqrt{\frac{E}{1-\nu^2}} \sqrt{\frac{W}{A}} \quad (10)$$

where E is elastic modulus and ν is elastic Poisson ratio may be used to estimate fracture toughness from W/A values obtained in impact fracture tests. The values of K given in Table 2 were estimated in this fashion. The values of K so obtained were consistently lower than those obtained from the hydrostatic tests and the fracture surfaces were less fibrous than those from the hydrostatic tests, which indicated some difference in fracture mode. Neither the impact fracture energy from the

un-nitrided specimens nor the estimated fracture toughness from the nitrided impact specimens appears to be simply related to the values of K calculated with Equation (9). In the absence of evidence to the contrary, it would appear that the scatter in load maximum values as given in Figure 5 and the effective fracture toughness values given in Figure 6 are the result of material inhomogeneity and experimental error rather than to variation in the average mechanical properties of the different heats of steel represented in the pipe specimens tested.

Based on the test data available at present, either of two methods can be used for reasonably accurate estimation of the limiting pressure load for a low carbon steel pipe containing an axial through-wall flaw:

1. Reference to a log-log plot of burst hoop stress versus ratio of flaw length to pipe diameter obtained from hydrostatic tests of a small number of flawed pipes of the material in question.
2. Use of Equation (9) and the value of K obtained from a few tests on pipes of the same material.

It should be possible to use the same techniques for estimating the behavior of pipes of austenitic stainless steels although it is questionable if Equation (9) could be used without modification to describe the behavior of these more ductile materials. Additional experiments with other materials and at other temperatures are needed.

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