

RAPID CRACK PROPAGATION in POLYETHYLENE
GAS PIPES

by

Paşa YAYLA

B.Sc. (Mech. Eng.), M.Sc.

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**Department of Mechanical Engineering
Imperial College of Science, Technology and Medicine
London SW7 2BX**

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ABSTRACT

Steady-state axial Rapid Crack Propagation (RCP) in pressurised plastic pipelines is a major concern of water and fuel gas distribution industries. Fast cracks are generally difficult to initiate, but once initiated, they may run through the entire length of pipeline at velocities of $70\text{-}370\text{ ms}^{-1}$ if the operating pressure exceeds a critical value. This critical pressure (p_c) is shown to be a function of material properties, pipe dimensions, and the properties of the pressurising fluid.

Although, for a given pipe system, the most widely trusted assessment of p_c is by the use of full-scale (FS) tests, these are expensive and laborious. A new Small-Scale-Steady-State (S4) test has been developed in order to define p_c for any piping system at a required temperature. The main features of the technique are: a fast crack is initiated by radial impact onto a well supported initiation zone. This crack penetrates a propagation zone along which internal gas flow baffles and an external containment cage against flaring suppress decompression and control the crack tip environment.

With this technique, p_c values for various low and high density PE pipes are well defined for a wide range of temperatures. Crack velocity remains constant during propagation, or decelerates abruptly in the arrest case. However, the p_c defined by this technique is extremely low, but on the basis of a gas dynamics model of RCP, a scaling factor is derived to correlate the S4 and FS test results. The effects of some important parameters (pipe wall thickness, pipe diameter, test temperature, and pipe material grade) upon RCP performance are studied. It is shown that the effect of each parameter defined from S4 test is in good agreement with the results obtained from FS tests.

Charpy impact energy results, on two different PE pipe grades for a wide range of temperatures, are presented and the relevance of these and the dynamic fracture resistance results of the High Speed Double Torsion tests are discussed. It is concluded that p_c is more sensitive to temperature than the Charpy results. The influence of the tear lips taking place at each free boundary of the pipe walls (particularly at the inner surfaces) is shown to dominate RCP resistance. The elimination of this highly ductile region by internal notching along the prospective crack significantly reduces p_c .

Quasi-static and specially developed dynamic finite element codes are employed to analyse RCP in PE pipes, focussing on the stress state around the crack tip. The axial stresses around the crack tip are generally higher than the circumferential stresses and stresses at the inner surface are higher than those at the outside.

Key words: plastic pipes, gas pipelines, PE, rapid crack propagation, critical pressure

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NOMENCLATURE

$\dot{a}$	: Crack velocity
a	: Crack length
A	: (i) Constant (ii) Cross sectional area
B	: Specimen thickness
b	: Distance
C	: Compliance
C_B	: A material constant (Eq.7.3)
C_V	: Charpy impact energy density
c	: Local speed of sound in the gas
c_l	: Longitudinal wave speed
c_o	: Speed of sound in the undisturbed gas
D	: Pipe nominal mean diameter
D_i	: Pipe inside diameter
D_o	: Pipe outside diameter
E	: Young's modulus
E_D	: Dynamic elastic modulus
E_S	: Static elastic modulus
E_k	: Kinetic energy
e	: Coefficient used in the Charpy experiments
F	: Force
G	: Energy release rate
G_D	: Dynamic crack resistance
k	: Constant
K_I	: Mode one stress intensity factor
K_{IA}	: Crack arrest fracture toughness
K_{ID}	: Dynamic fracture toughness

K_{II}	: Mode two stress intensity factor
L, l	: Length
M	: (i) The mass of swinging pendulum in the Charpy test (ii) Mach number (iii) Bending moment (iv) Bulk modulus of pressurising fluid (Eq. 7.17)
m	: The mass of the specimen used in the Charpy experiments
p	: Pressure
p_0	: Initial line pressure
p_l	: Line pressure
p_t	: Crack tip pressure
r_i	: Internal radii of pipe
r_o	: External radii of pipe
R_{avg}	: Pipe mean radius
R_s	: Circumferential distance measured from the crack tip
T	: Temperature
t	: Pipe wall thickness
v	: The impact speed selected in the Charpy experiments
v_c	: Crack velocity
W	: External work

GREEK SYMBOLS

Δ	: Displacement
θ	: Angular coordinate
ϕ	: Calibration factor used in Charpy impact energy calculations
γ	: Specific heat ratio of gas
α	: Angle
σ_z	: Axial stress
σ_H	: Circumferential stress

σ_R	: Resultant stress
σ_y	: Yield stress
ρ	: Density of pipe material
ρ_l	: Density of pressurising fluid

ABBREVIATIONS

AGA	: American Gas Association
ASIS	: American Iron and Steel Institute
ASTM	: American Society of Mechanical Engineering
BCD	: Battelle Columbus Division
BGC	: British Gas Corporation
BG	: British Gas
BMI	: Battelle Memorial Institute
FE	: Finite element
FS	: Full scale
CTOD	: Crack tip opening displacements
HSTD	: High speed double torsion
HDPE	: High density polyethylene
LEFM	: Linear elastic fracture mechanics
LDPE	: Low density polyethylene
MDPE	: Medium density polyethylene
PE	: Polyethylene
PMMA	: Polymethylmethacrylate
QS	: Quasistatic
RCP	: Rapid crack propagation
SCG	: Slow crack growth
SEM	: Scanning electron microscope
SwRI	: South West Research Institute
SS	: Small scale

INTRODUCTION

Natural gas is becoming a more popular fuel. Over the past two decades this fuel has become an effective and natural choice in the home for heating and cooking, and in industry for industrial heating, power generation and chemical processing. Consumption of this fuel for various purposes has been increasing every year. Pipelines deliver this effective fuel as and when it is required in the same way as the national electrical distribution system delivers electricity.

Until recently, coated steel pipes were almost the only choice for all installation networks, but lower oil prices and merciless competition among various alternative energy supplying industries has forced gas companies to find a more cost effective alternative distribution system.. Not only corrosion protection but high installation and maintenance costs forced gas companies to find a better pipe material to meet these requirements.

Plastic pipes, especially medium and high density polyethylenes (PE), have accommodated the demands of gas companies. This material has been widely used in Europe and the USA for more than three decades for both gas and water distribution. Today, about 95% of the piping system being installed is plastic. Most gas companies routinely install 8 inch (200 mm) diameter PE pipe, with 10 (250 mm) and 12 inch (300 mm) pipes becoming increasingly common. The largest size currently used in a USA regulated distribution system is 16 inch (400 mm). Although USA pipe manufacturers could already supply larger sizes, of diameter up to 48 inch, there is increasing concern about safety aspects of the use of higher diameter pipes in the distribution systems (Anonym, 1989).

One of the main drawbacks of these materials is that when a brittle fast crack is initiated by a 'third-party damage' or from 'stress concentrations', it may lead to so-called *rapid crack*

propagation (RCP) resulting in severe damage, posing a hazard to the public. When the operating pressure is above a certain value, called the *critical pressure*, an initiated fast crack could run at very high velocity, 100 - 350 m/s, in the axial direction over great distances along the pipelines. The danger of such a failure mode increases with increased pipe wall thickness, larger pipe diameter, lower operating temperature, and higher internal pressure.

In order to define the safe and allowable operating pressure, full-scale field tests have been carried out at a number of centres all over the world. However, these tests are not only expensive to carry out but also laborious. Dynamic fracture resistance and elastic modulus of the material need to be precisely characterized so as to apply fracture mechanics concepts to predict the critical pressure. Another problem is that a PE pipe has its own structural properties coming from the manufacturing process, and may not be characterized completely by making use of a small specimen to describe its service performance.

During the service operation, a number of RCP failures have been reported during the last years in PE as well as PVC pipes. In 1987, in Hungary a 700 m long RCP event appeared in a PE piping system which was operating at a pressure of 8 bar. Additionally, in Belgium in 1989, an RCP was initiated from a butt fusion in a $\phi 500$ mm pipe system and propagated for about 4m. In another RCP failure which took place in Belgium, the crack propagated about 40m (Vanspeybroeck, 1990).

The increasing use of plastic pipes at increasing diameters and operating pressures demands more sophisticated resins whose properties of slow crack growth (SCG) and RCP resistances must be checked by reliable quality control tests. Unfortunately, there seems to be no direct way of using measurable basic material parameters (such as melt flow index, yield strength, or density) in order to predict both SCG and RCP performances. SCG performance is conventionally assessed by accelerated environmental stress crack resistance tests, extrapolated to predict a 50 year design life. Since the damage and failure mechanism of these potential failure modes differ in many ways, a material with a good SCG performance does not necessarily have high RCP resistance, so that a safe pipeline system

must satisfy these two criteria independently. Since the time scale considered for SCG is 50 years, a number of reliable accelerated test techniques have been developed and used successfully to evaluate the performance of the pipe grade material.

The ultimate objective of this work is to develop a small-scale (SS) test technique to define p_c for a pipeline, given only a sample of its material, pipe dimensions (thickness t and diameter, D), and some knowledge of the contained fluid properties, (e.g. gas or air, water or water plus air). With this reliable SS test technique, the pipe manufacturers could easily and quickly assess the quality of their material, and the pipe users may effectively check their pipeline systems' RCP performance.

CHAPTER 1

LITERATURE SURVEY

1.1. Introduction

The advantages of polyethylene (PE) over metallic pipes for use by the gas and water transmission industries are numerous. They include the almost complete elimination of corrosion problems in service, as well as the ease and low cost of laying and installation. In the UK these advantages were recognised over two decades ago, and the conversion to PE for all new small-diameter installations is now complete. However, restrictions on the use of larger diameters are imposed by the greater possibility, and more serious potential consequences, of catastrophic propagating fracture.

For gas distribution, safety factors have been maintained in Continental European standards by limiting pipe dimensioned for 6 and 10 bar water pressure to 4 bar gas pressure (Benjamin, 1979), and to maximum diameters of 500 and 200 mm. In the UK stringent standards based on the more conservative 'strain energy theory' have limited pressures for the same Standard Dimension Ratios ($SDR = \text{outside mean diameter} / \text{wall thickness} = 17.6$ and 11) to just 2 bar. This straitjacket has become tighter with a trend to larger diameters, since pipe of this wall thickness becomes more difficult and expensive to produce, while the strain energy theory suggests that the safety factor will fall - but this theory is known to be conservative. Establishing exactly *how* conservative, and thereby safely surmounting these restrictions, could bring the user important advantages in efficiency and economy while further expanding the market for the suppliers.

1.2. Ductile and Brittle Crack Propagation

Pipe grade PE was originally of the High Density type (HDPE, of reference density $\geq 948 \text{ kgm}^{-3}$). The long, predominantly linear chains allow close packing and favour formation of

the strong crystalline phase. Smaller diameter installations used Low Density (LDPE), whose highly branched chain molecule and complicated structure weakens intermolecular bonding and prevents close packing, gaining ductility, flexibility and weldability at the cost of strength. More advanced copolymerisation techniques have led to the development of Medium Density MDPE (reference density $\geq 930 \text{ kgm}^{-3}$), actually a rather heterogeneous group of materials in which features of both HDPE and LDPE are retained by controlling and reducing the average branch length and conformation.

Although MDPE and HDPE pipe grades are, in general terms, very tough and ductile, the extruded product may fail by either ductile or brittle modes (Hertzberg, 1985). In normal environments high stresses (typically 13 MPa) are needed to activate ductile fracture, with energy-absorbent bulging and extensive plastic flow; such extreme conditions are easy to design against. The *regression* of yield stress with time is acknowledged in design procedures by extrapolation to an estimated pipe lifetime of 50 years, after which it has fallen to about 8.3 MPa (Ewing & Maine, 1980). At even lower stresses, however, more insidious brittle modes may become predominant after long times and/or in aggressive environments. Embrittlement can also be caused by exposure to thermal oxidation during processing, or to ageing in sunlight (UV radiation) before installation (Broutman, 1983; and Broutman *et al.*, 1985); even an extremely thin damaged surface layer can profoundly reduce the failure strain in tensile tests (up to 600% in virgin material), and precipitate a transition to brittle failure under normally acceptable static strains (*e.g.* due to earth movement or to rock impingement) during subsequent service.

The need to evaluate the resistance of specific pipe systems to brittle fracture, within a reasonable time scale, has led to the development of inexpensive and simple standardised Environmental Stress Cracking Resistance (ESCR) tests. In the *Compressed Ring* ESCR test a very short ring section is cut from the pipe, notched with a razor blade on the outside wall surface, and compressed at $\pm 90^\circ$ between steel plates to a constant strain in a warm detergent solution environment until crack initiation is recorded. Similar tests under constant loads performed by Lustiger *et al.* (1982) demonstrate a ductile-brittle transition, at some

characteristic time which the presence of the detergent solution drastically reduces. Correlations of ESCR with bulk material properties considered to be significant to service performance (e.g. the increased crystallinity associated with surface embrittlement) have supported its use as a quality control index.

From the viewpoint of a material supplier, Bragaw (1979) (of Du Pont of USA) has surveyed, using fractographic methods a large number of service failures from MDPE and HDPE gas piping systems. He distinguished *nine* fracture modes, of which creep crack growth, initiating at butt or socket joints and progressing with high local ductile drawing at or ahead of the crack tip, accounted for about half of the total. Less common modes include chemical attack, erosion fatigue, local pressure-generated overheating, electrostatic discharge, mechanical damage, hydrohammer damage, and 'mixed' modes such as squeeze damage. For gas distribution systems, fatigue is an unlikely mode of failure because of the compressibility of the medium - not so for water systems. It is also suspected that some service fatigue pipe failures have been induced by overhead traffic loading.

1.3. Rapid Crack Propagation

Common to all of these failure modes is their localised nature, facilitating isolation and repair of the leak. It is much more serious if, as in some of the failures observed in uPVC water distribution systems, local crack initiation somehow precipitates long-range axial brittle fracture. Such 'self-sustaining' (i.e. unstable) *Rapid Crack Propagation* (RCP) can be sustained at pressures which induce substantially less than the yield hoop stress for the material, and can propagate at 300 ms^{-1} or more for hundreds of metres, so that the economic benefits of understanding this failure mode so as to avoid over-conservative design are compounded by the high cost of repairing under-designed systems.

Impact cracks are marked by negligible ductility, since the high strain rate at the crack tip does not allow sufficient time for large-scale molecular mobility: the normally ductile amorphous phase is frozen to a glass (Bragaw, 1979).

Although the phenomenon of RCP in large diameter steel gas pipelines has been encountered on several occasions in the USA (Krishnaswamy *et al.*, 1986), it has not yet been reported for PE pipes in service. However, it can be induced both in full-scale simulations and in laboratory tests on PE pipes under certain out-of-specification operational conditions (Greig & Ewing, 1982; Greig, 1985a-b; Krishnaswamy *et al.*, 1987; and Gaube & Müller, 1980). Although these investigations have been invaluable in contributing to a general understanding of the problem (and provide reassurance that current systems are conservatively stressed), but their *ad hoc* use is extremely expensive, and have not succeeded in establishing a general solution or in validating a reliable design methodology. It is clear that much research remains to be done.

The organisation of this review broadly follows the engineering approach to any service failure problem. Firstly, we require reliable data and a broad phenomenological grasp of the failure process. In this case, service failures have not occurred, and data has come entirely from the *full scale simulations* which are surveyed in Section 1.4. Secondly, we turn to *small scale tests* on as-produced pipe, intended for quality control, or for determining pipeline behaviour in the laboratory without the effort and expense of sacrificing an entire structure. Here (Section 1.6) specific scaling problems arise due to the speeds involved and the time required to attain an initiation-independent steady state, raising serious questions of how representative these tests really are. Thirdly, a strong *theoretical model* is needed to translate confidently between the behaviour of a laboratory specimen and the behaviour of a full-scale simulation, accounting for any significant differences. For this problem virtually all investigators have based their language and concepts on those of Fracture Mechanics (Section 1.7). Finally, Section 1.7.2 reviews the proposed models for defining the critical pressure in both steel and plastic (particularly PE) pipes to sustain RCP.

1.4. Full-Scale Tests

A full-scale simulation of RCP in pipelines should fulfil three main requirements :

- i) it should accurately simulate installation, in particular reflecting specified welding and backfilling practice;
- ii) it should maintain the pipe in a realistic environment of temperature and load distribution; and
- ii) the pipe should be of effectively unlimited length.

The sheer scale and expense involved in setting up such simulations have restricted the number of investigators willing to do so. Only British Gas in the UK, and Battelle (sponsored by the Gas Research Institute) in the USA, have developed the necessary facilities.

Greig & Ewing (1982) reported the first results from tests using the BG facility. In preliminary work on small-diameter uPVC pipe, they had already noticed the existence of a *critical stress* below which a crack could be initiated but self-arrested, and above which it would continue to propagate. Their objective was to establish the critical stress for 250 mm SDR 17.6 MDPE and HDPE pipes under service conditions.

1.5. Critical Stresses

In the BG test bed, as illustrated in Fig. 1.1, a 24 m pipe length (three butt-jointed sections) is pressurised by air. To simulate service conditions, longitudinal stresses are reacted out by allowing end float within the fixed end caps, and the test pipe length is buried to 600 mm depth in sand except for a small area around the crack initiating groove, 3 m from the one end. The pressure is backed up by a steel gas reservoir pipe of 50 m length and 24 inch diameter, on the other end. This would be expected to minimise end effects from the decompression wave.

In early tests (Greig & Ewing, 1982) the temperature was not controlled. Some tests were conducted on standard yellow MDPE gas pipe in winter with an ambient temperature of about 3 °C, and at pressures ranging from 5 to 8.5 bar. The length of the crack jump, initially just 2 to 3 m, suddenly increased tenfold to the full test length at a critical pressure of 7 bar (hoop stress 5.8 MPa). Tests conducted in summer, with an average ambient temperature of 12°C, needed a much higher critical pressure in order to obtain propagating fracture: about 14 bar (hoop stress 11.5 MPa). The maximum crack length was then just 6m, of uniform sinusoidal fracture. In the spring (average ambient temperature 7°C) further tests were carried out on a black pigmented HDPE grade with a reputation for high RCP resistance. The pressure was varied from 7.5 to 14.5 bars, without achieving any significant crack propagation; most of the cracks arrested within 1m of initiation. It was not possible to increase the pressure much further without inducing circumferential yield. The high resistance of this HDPE grade to RCP was confirmed by butt-welding a 6m length of it to a 19 m length of MDPE pipe. RCP was initiated in the lower-resistance MDPE section and ran through the butt at high speed, but arrested within 2m in the HDPE section.

These initial tests illustrated a pronounced temperature dependence of the critical stress, a 10°C rise effectively eliminating RCP as a potential failure mode. Extrapolating back to 0°C (regarded as the UK below-ground operating limit), however, would have reduced the critical pressure to 4.8 bar (hoop stress 4.0 MPa), uncomfortably close to the BG proof test pressure of 3 bar. For more recent tests on yellow MDPE pipe carried out by Greig (1985a-b) the test temperature was maintained at 0 °C, and the critical stress fell to 5.1 MPa: not as bad as simple extrapolation predicted. However, the pipe, though the same grade, had been extruded two and a half years later, during which techniques may have improved. The influence of extrusion technique was demonstrated by tests on pipe produced by a process which redistributed the internal residual stresses by cooling both internally and externally. This increased the critical pressure by up to 30% for 250 mm and 14% for 315 mm pipe.

Internal pressure transducers were used to monitor the pressure decay, and fracture speed was measured by the crack breaking a sequence of conducting paint timing bands. Crack

speeds varied from 170 to 270 ms⁻¹ for the original material and from 100 to 220 ms⁻¹ for the new one. At high speeds the crack surface is featureless, while for lower speeds and for sinusoidal-pathed cracks surfaces were coarser, with striations which provided evidence for curvature of the crack front. Additional instrumentation yielded interesting and valuable information about the axial decompression process which accompanied crack propagation. In one test the crack tip initially propagated near the decompression wave speed (330 ms⁻¹), then slowed to half this speed before increasing again. Each phase took place at constant velocity, even when the initial crack front had gained several metres on the crack tip. After the crack tip had passed any point, decompression was completed in about 5 μ s.

RCP in PE pipes has a number of distinctive features. Generally, it is characterised by attainment of a *steady-state* above some critical pressure value. The brittle, almost glassy surfaces which the fracture leaves even in highly ductile pipe grade PE's, and the crack paths which are sometimes straight but often snake along an apparently sinusoidal path of strikingly constant amplitude and wavelength (Fig. 1.2) are the other features of RCP in PE gas pipes. Moreover the recorded crack propagation velocity is not constant but varies along the pipe. By contrast, the decompression wave front propagates almost at a constant speed (for air about 330 ms⁻¹) as shown by the BG's FS test results depicted in Fig. 1.3.

Based on the British Gas's database, the parameters controlling the RCP behaviour of pressurised PE pipes could be abstracted as:

i) **Temperature:** Decreasing the temperature of 250 mm SDR 17.6 BP 002-40 pipe from 12°C reduces the p_c significantly: from 14 bar, through 7 bar at 3 °C to 6.1 bar at 0 °C. Further data from tests at -20°C have shown that this rate of loss in strength is not sustained; the pressure falls only to 4.6 bar. This trend is plotted in Fig. 1.4

ii) **Pipe diameter:** For SDR 17.6 pipe, increasing the diameter from 125 mm through 250 mm to 315 mm causes the p_c value to decrease from 11.3 bar to 6.1 and 6.3 bar, respectively, shown in Fig. 1.5. As seen, there is a tendency that the

p_c decreases with the temperature. Note that the wall thickness is also varying (constant SDR of 17.6), so that the contribution of pipe diameter alone is not clear.

iii) **Pipe processing effect:** For Aldyl 'A' resin, the post-extrusion cooling process was found to affect RCP performance (the subject is discussed in more detail in Chapter 3). 250 mm SDR 17.6 pipe was cooled in two different ways: on the outside surface only (OLD), and on both outside and inside surfaces (NEW). Full scale RCP tests (Greig, 1986) showed that 'new' type pipe performed significantly better, although it is not clear whether the improvement in p_c is because of the change in residual stress profile (which changes from tensile at the inside and compressive at the outer side, to tensile at both inside and outside but compressive at the centre of the wall) or to the corresponding change in crystallinity profile.

iv) **Standard dimensional ratio:** Increasing the wall thickness for 250 mm BP 002-40 pipe, from SDR 17.6 to SDR 11, increases the p_c quite modestly: from 6.1 to 8 bar. Thus, a 60% increase in the thickness (from 14.2 to 22.7 mm) increases the p_c value only about 31%.

v) **Pipe resin:** This proves to be the dominant parameter controlling the RCP performance. Full scale type testing having recently qualified HDPE (TUB 125) 250 mm SDR 11 pipe to enter UK service at 7 bar rather than 3 bar as with MDPE (BP 002-40).

Greig (1984) also tested thicker (SDR 11) blue MDPE pipes completely filled with water throughout, and maintained at 3°C. RCP did not occur at all at pressures up to 21 bar, even when the initiation section was gas-pressurised, but RCP could be sustained within the test section at 13 bar if 5-10% of gas (air or nitrogen) was introduced above the water.

Krishnaswamy *et al.* (1986, and 1987) conducted a series of FS tests in Battelle. The pipe was 9.1 to 12.2m long, of diameter 8 to 10 inches and thickness 0.39 to 1.16 inches. The

internal pressure was varied from 2.2 to 13.6 bar and the temperature from -18 to 32°C, and various pipe materials were used; in total, some fifty specimens were tested.

All but one of the tests were conducted *above* ground (*i.e.* without backfill). The pipe was insulated with glass fibre, and cooled by circulating methanol coolant chilled by dry ice through it. On attainment of the required temperature, the methanol was drained and the pipe pressurised by air. No significant temperature rise was expected during pressurisation, and none was observed.

Most cracks either propagated much more than 1.5 m or arrested well within this distance. In some tests the pipe shattered, while in others the crack was observed to turn and propagate in the circumferential direction, and then either come to a stop or propagate right round the pipe, severing it (ring off). Pipe shatter and ring-off were classed as invalid tests by BG. In general, for a specific test, crack velocity remained fairly constant.

Initiating fast cracks in pressurised PE piping is a difficult job, on which experimenters have exercised much ingenuity. In service, even a violent pick-axe blow is unlikely to initiate fracture, although contact by a hydrohammer (a hydraulically driven soil tamper) has been known to initiate deep impact cracks (Bragaw, 1979). The lethal combination seems to be the generation of a sharp crack with high initial speed and sufficient length.

Bragaw (1980) reported a number of techniques which *failed* to do so, including, rather reassuringly, a simulated backhoe puncture of the sort widely feared by piping designers. The 2.5" wide backhoe tooth was one of several alternative striker designs fitted to a drop weight mass of up to 900 kg, and impacted at speeds of up to 8 m s⁻¹ onto 220-400 mm pressurised pipe. Both a dagger-like striker of 2.5" width and a wider (6") wedge-shaped chisel produced only local ductile punctures. Sharpening the vertical edges of this wedge as well as its leading edge, did, however, reliably generate rapid cracks. Filling the puncture produced by the standard wedge chisel with a degraded and oxidised weld polymer, and then re-pressurising was also successful.

Prabhat *et al.* (1983) prepared specimens by machining and razor-sharpening a long (254 mm) axial notch 80% through the wall. The pipe was then bound with fibreglass tape to prevent deformation while being pressurised. Initiation at full pressure was achieved by either suddenly cutting the tape with a knife edge, or by the detonation of a shaped explosive charge. This technique was later used by Krishnaswamy *et al.* (1986 and 1987).

Greig and Ewing (1982) initiated cracks by driving a 400 mm long blade into a pre-machined groove using an impact pneumatic cylinder. The initiation area was cooled by surrounding it with a mixture of solid carbon dioxide and methanol at -70°C (at normal temperatures the crack arrested by yielding and tearing at the end of the groove), and the pipe was supported internally to absorb the impact and prevent the propagation of initiation transients.

These full scale tests set well-defined parameters for the RCP problem: for a pipe of particular dimensions, and under specific conditions, there is a well-defined *critical pressure* above which a fast crack, once initiated, will propagate indefinitely, and below which it will arrest after minimal local damage. This critical pressure is material, processing and temperature sensitive. Such tests can certainly establish, almost beyond doubt, the safe operating conditions for a specific pipe system, but at the enormous expense of a series of go/no-go trials. The question now arises whether this single parameter can instead be measured or inferred from a small-scale test.

1.6. Small-Scale Tests

Unfortunately, inherent problems are raised by scale: the crack velocities, wave velocities and dimensions involved. A running crack has to start somewhere, but the ductility of PE at low strain rates makes static initiation very difficult. Some sudden initiation event (impact or explosion *etc.*) is normally necessary, but this radiates transient pressure and stress waves in all directions, at velocities generally exceeding that of the resulting crack; their reflections or virtual reflections are therefore destined to meet the crack tip again. A true steady state in which the crack tip has 'forgotten' its transient origins can only be achieved after the crack

- has become long relative to the scale of the impact event (knife or striker dimension), but has yet to meet boundary reflections of pressure waves, or has met a large number of them.

An established method of measuring threshold conditions of stress and/or temperature for crack arrest, for materials such as steel (and like PE) whose resistance to dynamic crack propagation is known to be much less than that to initiation, is the *Robertson crack arrest test* (Robertson, 1968). The test was one of the earliest crack arrest tests. As shown in Fig. 1.6, the test specimen is a plate with an ear at one end. This is stressed in a tension frame to some value below the material's yield strength. The notched edge of the specimen is cooled with liquid nitrogen to produce a temperature gradient across the specimen. A fast crack is initiated by an impact on the ear and runs until it encounters a temperature at which the material is sufficiently tough to arrest it. The value of the temperature at the crack arrest point is called Crack Arrest Temperature (CAT). Above this CAT the running crack cannot be propagated. From this test it has been shown that CAT depends on the material properties, the magnitude of applied stress and the dimensions of the specimen.

Vancrombrugge (1982) applied this basic idea of sending an artificially initiated high speed crack into a 550 mm long gas-pressurised pipe specimen (125 mm SDR 11). As in the Robertson test, a crack was initiated by a 50 J wedge impact in a notched, unpressurised section, cooled to -50°C, extending across an internal plug into the pressurised region from one end, Fig. 1.7. The pressure was varied from 4 to 8 bar and the temperature from 0 to 20°C. The impact crack usually accelerated on entering the pressurised section, but then sometimes decelerated or even arrested, perhaps due to depressurisation. As in the FS tests, there appeared to be a well defined critical pressure for a given test temperature below which no significant propagation occurred. This increased with temperature up to 15°C, and at 20°C there was no significant propagation at all.

This test method has been criticised by Williams (1984) on the grounds that for such a short specimen there can be no true transition from the initiation phase, driven and controlled by striker energy, to a steady state driven by internal pressure. Despite criticisms, the test has

been adopted as a standard in Belgium; the maximum allowed crack penetration into the gauge length at the operating pressure is one diameter.

Bragaw (1980) criticised the test on the grounds that since liquid nitrogen was used to cool down the crack initiation section and the fast crack was induced by axial impact, this initiated crack could have propagated some (unknown) distance into the pressurised zone. Thus, a resin with a very high impact resistance at liquid nitrogen temperature would form a relatively short impact crack length, on the other hand one with a lower liquid nitrogen impact strength would form a longer crack length. He also continued by arguing that for the same stress (or pressure) level one could get a different final crack length by selection of impact energy and initial crack velocity.

Most investigators who have adopted the spirit of this technique have used longer specimens (Krishnaswamy *et al.* 1986, 1987, and Wölters & Ketel, 1983). In a careful appraisal of scale effects, Wölters and Ketel (1983) compared results from 1m and 7 m 110 SDR 11 pipes, using the same low temperature initiation technique (110 J impact into a standard notch at -190°C). By measuring crack velocity, gas pressure decay rate and decompression wave speeds in the gauge length, they were able to show that the initial velocity of the crack exceeded the speed of the decompression wavefront (about 400 ms⁻¹) - clearly a desirable attribute for the test. Arresting cracks were more easily distinguished from propagating cracks by examining their speed rather than by measuring the final crack length: this declined monotonically after initiation (but could still survive the length of the specimen), whereas longer-propagating cracks stabilised quickly (within 200 mm) to a constant velocity of 100-200 ms⁻¹. They also confirmed the common observation that steadily propagating cracks tend to follow sinusoidal paths, while arresting cracks straighten out to run axially. Tests on longer pipes correlated very well with the 'Modified Robertson' (MR) tests. Cracks which arrested did so within about 15-20 diameters, much greater than that allowed by the Vancrombrugge test, which can therefore be regarded as conservative.

Krishnaswamy *et al.* (1986 and 1987) conducted a very extensive series of similar laboratory scale tests, which they referred to as MR tests. The modifications included increased length (1.5 m, chosen because in the full scale tests most cracks either propagated much more than or arrested well within this distance), the use of expansion plugs to seal the pipe, and the use of an axial pendulum-impacted wedge, opening a sharpened notch for initiation. The parameters investigated, and the procedure were the same as for their full-scale tests (see section 1.5). In all, 52 tests were carried out on pipes ranging in diameter from 6 to 24 inches. Each pipe was cooled to about 5°C below the test temperature for 30 minutes in a methanol and dry ice bath, then removed from the bath, located in the rig and covered with thermal insulation. The notch and wedge were cooled to below -40°C for three minutes by spraying with liquid nitrogen. The pipe was then pressurised and, at the required test temperature and pressure, the hammer was dropped onto the wedge, arriving with a total kinetic energy of about 110 J.

In general, crack paths were similar to those observed in the FS tests (except that no pipe shattering was observed), but it proved to be quite difficult to define whether or not true RCP had occurred. An admittedly arbitrary criterion was adopted: propagation for more than 2 diameters. The investigators recognised the uncertainties inherent in short-pipe testing due to end reflections of stress waves in the pipe material as well as of the gas decompression front, and tests in which the crack was judged to have arrested prematurely due to decompression were classed as RCP. Tests in which the degree of interaction could not be estimated were considered invalid. A further criterion for crack initiation was specified: the attainment of a crack velocity at or above $0.8\dot{a}$ where;

$$\dot{a} = c_l \sqrt{\frac{2}{(SDR)}} \quad (1.1)$$

c_l being the longitudinal wave velocity for the pipe material. However, crack velocities were not always measured in the MR tests but were estimated from FS tests on the same materials under similar conditions. Another source of uncertainty recognised by the Battelle

researchers was the elevation of hoop stress by expanding internal sealing plugs; both a longer specimen and a modified plug design were recommended to other investigators.

Gaube & Müller (1980, and 1983) of Hoechst, West Germany conducting similar tests to investigate RCP in gas pipelines, used even longer specimens: 5 m. They tested SDR 11 pipe of outside diameters 90, 180, and 400 mm, in two HDPE grades, A and B, having different toughnesses as measured by Charpy tests at 0, 10 and 20°C. The toughness of material B was greater and, across this temperature range, virtually remained constant.

One end of the pipe specimen was connected to a steel reservoir charged with air at up to 10 bar, and the other end capped. The test pipe was enclosed in a wire cage, for safety. The crack was initiated by a weight falling freely down through a vertical tube to strike a knife resting on the pipe 1 m from one end. In other tests the cracks were initiated 'statically', by sawing a longitudinal cut and pressurising until a crack grew from it.

RCP was defined by crack propagation exceeding 2 m. RCP was not seen at all in the tougher material (the maximum crack length never exceeding $2D$), but was seen in some tests on material A. The critical pressure p_c was shown to decrease with increasing D . No significant difference could be detected between static or dynamic crack initiation, but increasing the knife length from 20 to 220 mm did seem to reduce p_c . Gaube and Müller concluded that, since in practice HDPE pipes were not subjected to pressures as high as 10 bar, there was little justification for a 250 mm diameter restriction at standard pressures.

Wölters (1985) performed small-scale tests on 1m long specimens as well as a few virtually full-scale tests (5 to 7 m) on 110 mm SDR 11 pipe. Using timing wires painted onto the specimen allowed a slightly more accurate estimate to be made of the crack velocity even from short specimens: after high-speed initiation the crack always decelerated, but sometimes restabilised to a constant velocity rather than arresting. In long specimens the crack might still arrest after only limited propagation (perhaps on arrival of a reflected decompression wave?) or propagate through the whole test length. The existence of a velocity plateau (usually at about 150 ms^{-1}) in a small specimen was considered sufficient to define RCP,

and with path lengths generally exceeding 500 mm transient effects of initiation could perhaps be neglected. Testing two PE grades at 0°C revealed a critical pressure of only about 2 bar for one but of greater than 10 bar for the other. In tests on 90 mm pipe, raising the temperature raised the critical pressure; so too did annealing, though assessment of a pipe treated to simultaneous internal and external cooling after extrusion did not reveal the benefits which British Gas full-scale tests had done (Greig, 1985-b).

Reviewing the available body of data from various sources on RCP, Krishnaswamy *et al.* (1986) reached the rather discouraging conclusion that comparisons could not be made because crack initiation techniques varied. If extended, this argument would invalidate any test method, since initiation under service conditions is not controlled, but is assumed to exert only local effects. It is nevertheless clear that the development of a representative small-scale (modified Robertson) test must carefully *isolate* the initiation event from the test region, and must somehow attempt to promote the stable state which the reflection of impact stress and decompression waves otherwise quickly destroys.

1.7. Theoretical Models

1.7.1. Theory of Fracture

Although fracture problems have been reported for centuries (Broek, 1974, and Kanninen & Popelar, 1985) a description and foundation of fracture mechanics development starts with the work of Griffith (1921). Even though his energy balance approach is not the one ultimately accepted for the modern fracture mechanics calculations, it is related to the crack-tip field approach that had its factual beginning with the work of Inglis (1913) and is based on the stress and strain analysis of the structure containing crack like flaws. This analysis defined the crack-tip *stress intensity factor*, K , as a single-parameter fracture criterion.

It was in the field of impact behaviour that the earliest systematic attempts to characterise the toughness of materials were made. In the early 1900's several investigators developed impact bending tests, in which a notched bar of specified geometry was struck by a

pendulum drop weight whose loss in energy, appropriately corrected, was attributed to the work required for fracture. The most popular of these tests is that devised by Charpy. If specimens of various sizes are tested, the absorbed energy can be referred to the area of the fracture (on one face only) to yield a *specific impact energy*. This quantity can be explored as a function of temperature, strain rate (*i.e.* impact velocity) and thickness to reveal transitions in behaviour from ductile to brittle, as well as for the comparison of materials under similar conditions, but the test remains qualitative in the sense that the results cannot be used to predict the behaviour of geometrically dissimilar structures.

Then Griffith and others later developed the idea that the *crack resistance*, R , the work required to form unit area of crack surface, might be a material property. Since R can be visualised as the force per unit length exerted on an imaginary cutting front (a sort of imaginary cheese wire), this implied a *crack driving force*, G , which must attain a critical value G_c equal to R for the loaded crack to propagate.

1.7.1.1. Linear Elastic Fracture Mechanics (LEFM)

The application of LEFM to polymers is well established (Williams, 1984). Although PE is, in general, non-linear and rate-dependent in its elastic response, it can be well modelled as a linear, isotropic homogeneous and continuous solid at the time and size scales relevant to RCP. Within these restrictions, LEFM relates the crack driving force to the externally applied load, F , and its displacement, Δ , or equivalent stress-area or pressure-area products, by one of two fundamental approaches:

i) **Energy approach:** The First Law of thermodynamics can be applied to a defined system within which all components of energy (kinetic, strain *etc.*) can be estimated, and into which the work input can be calculated. If, for example, kinetic energy changes are negligible, then:

$$GB_c = \frac{1}{2} F^2 \frac{dC}{da} \quad (1.2)$$

where a is the crack length, B_c is the crack path width, and

$$C = \frac{\Delta}{F} \quad (1.3)$$

the compliance.

ii) *Stress analysis approach*: The severity of the crack tip opening stress field can be measured by a single parameter, K , the *stress intensity factor*, and calculated directly via a stress analysis. It can be shown that :

$$K^2 = E G \quad \text{under plane - stress conditions} \quad (1.4)$$

$$K^2 = \frac{E G}{(1 - \nu^2)} \quad \text{under plane - strain conditions} \quad (1.5)$$

Clearly K and G are effectively interchangeable, and, for example, a critical K value K_c will drive crack propagation against a crack resistance

$$R = \frac{K_c^2 (1 - \nu^2)}{E} \quad (1.6)$$

When comparing the toughnesses of different materials, however, care must be taken to note the character of each parameter. Marshall & Birch (1985), comparing pressure pipe grade polymers, quotes K_c values for slow crack growth of 1.0 to 2.0 MPa m^{1/2} for MDPE and 3.0 to 4.0 MPa m^{1/2} for uPVC. Since their low-rate moduli are so different, about 0.7 and 2.8 GPa respectively, these materials have rather similar R values.

The distinction between plane stress and plane strain driving forces is usually swamped by a change in the material's crack resistance. A thin sheet with a short crack front exerts little constraint on local plastic flow at the crack tip, allowing it to blunt, relaxing stresses and raising R . Such plane stress fracture surfaces, showing shear lips near the free surfaces and evidence of ductility, illustrate the dominance of shear stresses. As the specimen thickness rises towards a critical thickness value:

$$B_c = 2.5 \left(\frac{K_c}{\sigma_y} \right)^2 \quad (1.7)$$

R falls towards a plateau value, the flatter and glassier fracture surfaces illustrating that less work was needed to create them. Factors increasing the yield stress σ_y (such as higher strain rate and lower temperature) reduce the transition thickness and promote plane strain conditions. For MDPE, with a σ_y of 13 MPa, this thickness is at least 30 mm.

In problems involving propagating cracks, it must be recognised that R may be a function of both crack length, a , and crack velocity $\dot{a}$. The 'R-curve' for metals shows that they tend to require greater work for initial formation of the crack-tip zone than for its subsequent propagation. The resistance of rate sensitive materials such as thermoplastic polymers generally rises with crack speed, although 'stick-slip' crack propagation in some thermosetting polymers is attributed to a falling characteristic. While the very existence of a unique $R(\dot{a})$ characteristic is still under discussion, and remains undetermined, it is common to distinguish between *initiation* and *propagation* toughness, and to recognise rate sensitivity.

Thus the LEFM approach separates the pipeline fracture problem into two phases. Firstly, the crack propagation process must be analysed (using either a stress analysis or an energy method) to compute the crack driving force. Secondly, an appropriate specimen must be selected within which similar crack behaviour (*i.e.* similar velocity and constraint) can be induced, under a controlled driving force, in order to measure the pipe material's crack resistance, R .

1.7.1.2. Crack Arrest (Dynamic) Fracture Mechanics

The subsection of fracture mechanics concerning rapid crack propagation, usually named 'dynamic fracture mechanics' has been attracting continuously increasing attention of the scientist and design engineers involved in the design of nuclear pressure vessels, gas and off-shore oil pipelines, dynamic comminution, geological formation related with earthquake, ship hulls, cryogenic storage tanks and railroad rails. Thus the progress on dynamic fracture

mechanics is the concern of international conferences (Sih,1973; ASTM STP 627, 1977; ASTM STP 711, 1979 and more recently ASME Pressure Vessels and Piping Conference, *Dynamic Fracture Mechanics for the 1990's* (1989) and special issues in international journals (Int. J. of Fracture, Vol. 27, Nos. 3-4).

Although it is possible to design an engineering structure so that a fracture will not be initiated under normal service conditions, the possibility of fracture being initiated in the structures has to be taken into consideration. Designing against this damage is impossible, but the main philosophy should be that once such a damage happens to the structure it will localise and not propagate long distances. The study of this question led to the development of the crack arrest toughness, K_{IA} , concept which not only demonstrated crack arrest behaviour but measured the value of the K_{IA} parameter.

It has long been known that the resistance of some materials to crack propagation is below that of crack initiation. Thus RCP is governed by an equality between the dynamically computed crack driving force, K , and the material's resistance to crack propagation which is a material property that depends upon the crack speed ($\dot{a}$) temperature (T), and thickness (t). This is given as (Kanninen *et al.* (1990)

$$K (a , \sigma , t) = K_D (\dot{a} , T , B) \quad (1.8)$$

where K is a function of crack length (a), applied stress (σ), time (t), and specimen configuration and its dimension. Crack arrest occurs as K becomes less than the minimum value of K_D .

1.7.2. Simple Models of Pipe Fracture

1.7.2.1. Strain Energy (British Gas) Model

A simple LEFM analysis of continuous RCP along a pressurised pipeline proposed by Greig & Smith (1973), and Harrison and Smith (1973a-b), has gained wide use (Greig & Lwing, 1982, Gaube & Müller, 1980). It is based on the energy analysis of a steady state with

constant crack velocity. At a control volume boundary some fixed distance ahead of the crack tip, the internal pressure generates a hoop stress $pD/2B$, where D and B are the pipe diameter and wall thickness, so that the strain energy per unit length of pipe is:

$$U_c = \frac{\pi p^2 D^3}{8 E G} \quad (1.9)$$

At a fixed, large distance behind the crack tip, the hoop stress and thus the strain energy are assumed to have fallen to zero. Thus a crack extension da draws strain energy $u_c da$ into the control volume and dissipates energy $G_c da$ at the crack tip. Neglecting all other terms in the energy balance yields:

$$G_c = \frac{p_c^2}{B^2} \frac{\pi D^3}{8 E} \quad (1.10)$$

so that

$$p_c = \frac{B}{D} \sqrt{\frac{8 E R}{\pi D}} \quad (1.11)$$

specifying, as sought, a critical pressure below which an axial crack cannot propagate steadily through a material of crack resistance R . Translation to K_c simplifies this equation to

$$p_c = \frac{B}{D} K_c \sqrt{\frac{8 (1 - \nu^2)}{\pi D}} \quad (1.12)$$

The steady-state energy analysis, by ignoring mechanics, answers doubts such as Greig's (1985-b) that the strain energy released by any pipe ring could not be made available immediately for fracture work: indeed it cannot, but the energy balance integrates energy released during all previous time as well.

British Gas developed tentative design rules based on this Fracture Mechanics approach to uPVC (Smith, 1971a-b) and later PE pipe design before building their field test facility to test them (Harrison & Smith, 1973-b). K_c was measured from instrumented Charpy impact

tests: about 2 MPa m^{1/2}, varying little with temperature between 0°C and 20°C. For this value the propagating-fracture stress fell to the hydrostatic design stress at just over 400 mm diameter.

The steady state analysis leads to a formula for the crack driving force in which the crack length does not appear. This is not, as Bragaw (1980) implied, because it has been incorrectly derived from a 'total energy' rather than an energy-derivative analysis, but because it models a steady-state process in which the total energy in the system is irrelevant (and is in fact infinite). For a short crack the situation is rather different, since no control volume can be defined outside which energy changes are negligible. The driving force for a short crack in a long pipe will be very close to that for one located centrally in an infinite sheet:

$$G = \frac{\pi \sigma^2 a}{E} \quad (1.13)$$

which will reach the critical value to drive a steady-state crack at a crack length of $2a=D$. This suggests superficially that any crack one diameter long which can propagate under the driving force of the wall stress *only* will continue to self-propagate indefinitely. There are several objections to this simple view.

Note that it is implicit in this argument that the crack is propagating under the driving force available from the pipe wall stress only. In practice some additional impact event is needed to start a crack, and may still be contributing substantially to its driving force until the crack is substantially longer than one diameter; for example, a chisel driven into the pipe can exert a continuing wedge-opening stress component even after depressurisation (Bragaw, 1980). On the other hand, radial displacements under such a blow will propagate circumferentially and axially, with periodic phase changes, and may therefore contribute a transient crack-unloading component later on.

At Battelle (Krishnaswamy *et al.*, 1986), this expression was supplemented by an estimate of G for a shorter crack, by modelling it as a crack in a wide plate under the pipe hoop stress.

The results from both full- and laboratory-scale (Robertson) tests were then analysed statistically on a G versus R basis, using R data measured from Charpy tests. The survival confidence limits were often very widely spaced, reflecting various uncertainties: in particular in the short crack G estimate applied to Robertson tests, which appeared to be markedly inflated. The extent to which the simple model had to be stretched to accommodate the data is suggested by the concluded Battelle design equation for 95% survival confidence: not $G=R$, but $G=0.055 R^2$ (with dimensions in in-lb units, R being typically 5-15).

Gaube & Müller (1980), whose laboratory-scale tests were described above, found better formal agreement with the BG relationship, attributing discrepancies in the actual level of p_c to over-simplifications in its derivation. Later Brostow & Müller (1986) developed an equation relating the notch impact energy U determined from Charpy tests at 0°C to the final crack length, a_f , after RCP in 110 mm diameter PE pipes at the same temperature; this is, not surprisingly, an inverse linear relationship with two undetermined empirical constants. The authors then introduced a limiting energy value for a crack to form, pointing out that negative values of a_f may be interpreted as crack arrest, and attempted to connect this equation with the macromolecular chain behaviour of the pipe material. It is difficult to see the usefulness of this approach in dealing with the full-scale pipe fracture phenomenon, whose essentially steady-state character renders a_f uninterpretable. It tends instead to emphasise that laboratory-scale tests on short lengths of pipe are rather similar to Charpy tests in providing a finite reservoir of energy, of which some characteristic proportion is expended in fracture, whilst the gas pressure ahead of a steadily-running crack in a long pipe is effectively an *infinite* energy source. Interestingly Vancrombrugge (1982), the originator of the Robertson short-pipe test, was unsympathetic to LEFM.

Greig (1985b) has found the strain energy expended in fracture to be only 21-36% of that apparently available: in other words, either the driving force or the crack resistance is being over estimated by a factor of about three. There has been some discussion of the relevant modulus value. Kanninen *et al.* (1987) holds that since strain energy ahead of the crack was originally gained by slow pressurisation, and is completely lost, the relatively low 'static'

modulus should be used. An alternative viewpoint suggested by the same group is that strain energy is *released* much faster, and therefore at a much higher modulus, the excess over that originally acquired being dissipated in unknown proportions in near-tip work to aid fracture, and in the bulk material, merely generating a small temperature rise. The small proportion observed by Greig corresponds closely just to that released directly, due to an increase in modulus from low to high rates by a factor of nearly three.

1.7.2.2. Simplifying Assumptions in the Strain Energy Model

More advanced pipe analyses can be introduced by assessing the simplifying assumptions made in this one, which are as follows.

- a) The change in internal energy of the contained fluid is neglected, *i.e.* the fluid does no work. This is equivalent to assuming either that the crack does not permit depressurisation, merely allowing the containing wall to relax, or that the initial energy of the medium was negligible. For an almost incompressible material like water the strain energy per unit length will be

$$u_f = \frac{1}{2} \frac{p^2}{M} \frac{\pi D^2}{4} \quad (1.14)$$

which, for water with a bulk modulus $M=2.3$ GPa, similar to the high-rate modulus for HDPE, turns out to be roughly the pipe wall elastic strain energy divided by the SDR; to neglect this is acceptable, given the overall climate of approximation. The internal energy of a compressible gas is so much greater as to be effectively infinite, but its potential contribution to fracture work is limited by the degree to which pressure flares the pipe walls radially outwards as they are freed by the crack.

- b) All of the dissipated energy is considered to have been absorbed in fracture. Other possible energy sinks include ductile deformation of the pipe walls (plastic stretching and flaring) and deformation of the resistive backfill. For HDPE the former is probably negligible: fracture is brittle in the sense that the pipe fragments

can usually be pieced together again without revealing plastic deformation. The backfill contribution is difficult to assess.

c) Kinetic energy is neglected. Although considering sufficiently remote control volume boundaries justifies this assumption, it also begs the more difficult question of where the K_E associated with fast pipe relaxation and gas flows has been dissipated. In practice K_E of pipe wall vibrations, and perhaps also of ejected backfill, may be significant.

d) Only the hoop stress contribution to elastic strain energy is accounted for. Significant contributions may also arise from bending (due to radial displacement gradients around the circumference) and from axial shear (due to circumferential crack face displacement gradients along the pipe axis).

e) No account is taken of residual stresses, which may make a significant contribution to the crack driving force (Vancrombrugge, 1983 and Williams *et al*, 1981); annealing can considerably increase p_c in Robertson tests (Wölters, 1985).

f) Only axial crack propagation has been considered. Pipelines may fracture with a variety of different fracture paths. Circumferential oscillations may be superimposed on an axial path. Circumferential propagation, severing the pipe at an axial plane, and helical paths have also been observed.

g) Residual stresses: medium to high density polyethylenes are quite highly crystalline, and shrink considerably during crystallisation and cooling. Extruded pipe normally passes through a sizing die into a water bath in which it is cooled from the outside, so that interior layers shrink while cooling onto adjacent outer layers, putting them into compression. The net residual stress pattern varies from biaxial tension on the bore to biaxial compression on the outside. Injection moulded fittings, on the other hand, are cooled on both surfaces, leaving them in compression while the core remains in tension (Wong & Broutman, 1983). Some control over both the

magnitude and the distribution of residual stresses can be exerted during processing: very slow cooling promotes relaxation.

h) **Crack shape:** It is well known that fracture surfaces in polymers often show a network of *arrest* lines (marking instantaneous positions of the crack front) and *river* or *hackle* lines which mark the direction of movement of the front; thus where both are present they cross at right angles. Arrest lines are less common on fast fracture surfaces than hackle lines (Bragaw, 1979). The latter demonstrate that the crack front shape is markedly curved.

i) **Pressure decompression:** The assumption embodied in the theory – that *all of* and *only* hoop strain energy due to the line pressure drives the crack – can be questioned by reference to a simple model: decompression model. In reality, gas will flow back steadily from the undamaged section to the failed section. As a result the pressure level at any point behind a decompression front will be less than the line pressure for crack velocities $\dot{a} < c_0$, and the pressure at the crack tip is given by Maxey (1974 and 1979) as

$$p = p_l \left[\frac{2}{\gamma + 1} + \frac{\gamma - 1}{\gamma + 1} \frac{\dot{a}}{c_0} \right]^{\frac{2\gamma}{\gamma - 1}} \quad (1.15)$$

where p_l is the line pressure, and γ is the specific heat ratio of the gas. If the philosophy of the strain energy theory is correct, not the original line pressure but the reduced crack tip pressure has to be taken into account. As this reduced crack tip pressure is considered the critical pressure would depend on the crack velocity $\dot{a}$ and the strain energy model has to be modified as;

$$p_c = \frac{t}{(D - 2t)} \sqrt{\frac{8EG_D}{\pi(D - t)}} \left[\frac{2}{\gamma + 1} + \frac{\gamma - 1}{\gamma + 1} \frac{\dot{a}}{c_0} \right]^{\frac{-2\gamma}{\gamma - 1}} \quad (1.16)$$

Only a crack propagating with a velocity $\dot{a} \geq c_0$ will be unaffected by the pressure decay behind it.

1.7.3. General Fracture Propagation Models

In order to establish a design criterion or criteria for crack arrest in pressurised pipeline in terms of material properties, pipe dimensions, pressurising medium properties, etc, full-scale burst experiments have been conducted by Battelle Columbus Laboratories, (BCL), (Eiber, 1969), the British Gas Council (BGC) (Poynton & Fearnough, 1973), the American Iron and Steel Institute (AISI) (Ives *et al.*, 1974), SCM/Italsider of Italy (Bonomo *et al.*, 1980) and a Japanese consortium (Sugie *et al.*, 1985). Beside these full-scale tests a number of models have also been proposed for ductile crack propagation and arrest in steel pipes. These models could be summarised as follows:

a) Energy Balance Approach: The approach was proposed by BGC (Fearnough *et al.*, 1971). The approach deals with the energy balance between the energy supplied by the escaping fluid and the energy dissipated as fracture energy as well as kinetic energy and other energy sinks, such as stretching energy and backfill. The approach was then used by various investigators to study rapid ductile crack propagation in gas pipelines. These are: Poynton & Fearnough (1973), Alder *et al.* (1984) and Abbassian (1985).

The major difficulty in this approach, as pointed out by Shoemaker & McCartney (1974), is that the total energy in the gas is very large compared with the energy associated with the fracturing process. In addition to this, the calculation of other energy sinks is not easy. Thus, it is possible that inaccurate approximations of these larger energy terms may put in shade the changes which are being considered as contributing to the fracture event. On the other hand the energy method is attractive because it avoids the need to consider the details of the stress and deformation fields and is only function of the crack velocity.

b) Statistical Analysis: This approach is based upon the statistical analysis of full-scale tests considering all possible parameters and arriving at an equation that

could be used to define the boundary between crack arrest and propagation. This method has been used by a number of investigators: Hahn *et al.* (1969), Maxey *et al.* (1974), Shoemakers (1980), Sugie *et al.* (1982). Using these empirically determined equation(s) the minimum Charpy energy needed to prevent ductile fracture propagation in a gas transmission pipeline has been determined. All these empirical equations were given in the study of SwRI (Kanninen *et al.*, 1989).

Since these empirical relationships have been constructed to fit the available full-scale test database there is a poor discrimination for conditions that are outside the original database. Thus, some discrepancies exist among these approaches. The reasons for these inconsistencies were delineated in SwRI report (Kanninen *et al.*, 1989) which pointed out that the Charpy absorbed energy is not a valid fracture toughness measure. Moreover, for larger diameter pipes and higher pressures that are beyond the database results, the models could deviate from each other in their prediction.

1.7.4. More Advanced Pipe Models

More sophisticated models for dynamic crack propagation along pipelines, dealing with at least one of the issues outlined above, have exercised numerous investigators and generated a large body of literature. Here only a representative selection of these will be mentioned; these are oriented towards the fracture of ductile metal pipes, since only relatively recently has attention turned to the peculiar problems of analysing plastic pipes.

1.7.4.1. Dynamics of the Pressurised Fluid

The dynamics of the fluid escaping from a fractured pipe can most simply be modelled in one dimension by assuming an infinitely long pipe to have been guillotined at some fixed initiation point, allowing free choked outflow there at ambient pressure, as shown in Fig. 1.8 (Wölters, 1985, and Maxey *et al.* 1972). Subsequent axial pressure profiles are exponential in form, extending with time between the decompression front, advancing at sonic velocity at the initial pipe pressure, and the stationary initiation point at ambient pressure. The pressure

decay history at every fixed point along the pipe is identical, but the section at which pressure has fallen to any particular level advances at some characteristic subsonic velocity. Thus the higher the velocity of a steadily propagating crack front, the higher the pressure and axial pressure gradient which it will experience. Whereas Krishnaswamy *et al.* (1986) visualised the crack arresting if it failed to keep pace with the overall decompression speed, this model suggests interaction between a velocity-dependent driving force and, perhaps, a crack-speed dependent crack resistance, permitting steady propagation at a range of subsonic velocities.

While the assumption of free outflow from the entire pipe section, realistic for ductile fracture with wide flaring of the crack faces, might seem more dubious for brittle fracture, some observations from British Gas full-scale tests outlined earlier do support this picture. Greig (1985-a) addressed the question of why a crack could propagate at half the decompression speed, apparently driven by a hoop stress considerably less than the minimum which would normally be necessary for 'in-phase' propagation (*i.e.* on the crest of the decompression front). His view is that the material presents a lower crack resistance at the lower velocity. It is interesting to note that for a crack to run *stable* at any constant velocity, impervious to the inevitable fluctuations, it is necessary for the crack to accelerate if it falls back from its equilibrium point on the decompression wave, and to accelerate if it runs ahead.

For liquid-filled pipes the initial decompression wave is strongly coupled to deformations of the pipe, so that its speed c depends on pipe dimensions and modulus as well as on the density ρ_l and compressibility of the fluid:

$$c = \sqrt{\frac{M}{\rho_l} \frac{1}{1 - \frac{M}{E} (SDR - 1)}} \quad (1.17)$$

where M is the bulk modulus of the fluid (for water 2100 Nmm^{-2}) and E is the modulus of the pipe. Thus in water-filled SDR 11 HDPE pipes much higher crack velocities must be sustained ($c=400$ to 500 ms^{-1}) to keep the crack tip on the initial decompression wave front; on Greig's argument this may contribute to the much higher resistance to RCP of water-filled

pipes. The more realistic situations of an air-water mixture (partially filled water pipe) or localised air inclusions (*e.g.* at pipe joints, Kottman & Lutz, 1983) remain interesting and almost unexplored areas.

1.7.4.2. The Split Ring Model

Building on experience gained by direct elastic finite-element shell analysis (Emery *et al.*, 1976) of the axial pipe problem, Emery *et al.* (1981-b) introduced a more sophisticated fluid dynamics model while preserving many other simplifying assumptions. Fluid decompression by progressive outflow between the crack faces is coupled to the pipe wall deformation to compute the crack driving force. The length of the pipe is discretised as a series of independent rings on the assumption that wall stresses are small enough for these to deform independently of each other: a reasonable assumption for sufficiently thin-walled pipes. Ahead of the crack tip the circumferential strain and radial displacements are neglected, and cohesive forces are imposed on the crack line. Behind the crack tip, circumferential and radial strains remain zero, but the crack faces separate by an amount equal to the average radial deflection. The axial strain is zero everywhere.

By calculating the crack opening displacement history for the ring immediately behind the crack tip, and relating it to a fracture criterion, the crack tip extension history can be computed. The opening displacement of the other rings was used to define the fluid outflow area and to compute the pressure history. This *split-ring* model was originally used together with the one-dimensional fluid flow analysis, for modelling ductile axial crack extension in metallic pipes pressurised by air, hot water and cold water. Cracks propagated only in the air-pressurised pipes.

Computations for the dynamic small-deflection motion of a ring, of constant thickness, subjected to time-varying internal pressure (linear decay) showed the opening displacement acceleration to be directly proportional to pressure. Using the model in conjunction with measured average pipe pressure histories to compute crack extension speeds and crack openings produced good agreement.

Emery (1981-a) went on to use this model in combination with a dynamic finite element method, to investigate different pressure histories as well as different pipe cross-sections (including sections of non-uniform thickness). The crack opening acceleration was still found to be proportional to the pressure level. It was also concluded that, in a very ductile material, the motion of the crack is mainly affected by the inertia of the moving mass around the crack tip.

1.7.5. Semi-Empirical Models for RCP in PE Pipes

Using the fracture mechanics principles to analyse both their full-scale field tests and small-scale test results from the Modified Robertson test, Battelle Columbus Division (BCD) has carried out a research program to investigate RCP in PE pipes. Moreover, using the BCD results, Southwest Research Institute (SwRI) has developed an elastodynamic finite element analysis and applied it to determine the crack driving force associated with steady state crack propagation in PE pipes. The common objective of these investigations was to develop a simple design procedure to preclude RCP. These two projects were sponsored by the Gas Research Institute of the USA.

1.7.5.1. Battelle Columbus Division (BCD) Model

On the basis of the quasistatic LEFM principles, BCD has correlated their FS and MR test results using the dynamic effective modulus measured by the waterhammer test and a measure of dynamic toughness obtained with modified Charpy test results. The tests and correlation analysis were made for the test parameters; temperature, diameter, and thickness. Based on this correlation a new design procedure has been proposed to predict the maximum allowable pressure below which large-diameter PE gas pipes will not sustain RCP.

$$G = \left(\frac{\pi}{4} \right) \frac{1 - \nu^2}{E} a p^2 (SDR - 1)^2 \quad (1.18)$$

where a is the half length of the flaw (in inches) that the pipe material is assumed to contain, E is the elastic modulus which was taken to be a dynamic quantity determined from

waterhammer tests. From the extensive waterhammer tests on different lots of PE, test results were then analysed and the dynamic modulus was generalised in terms of test temperature as;

$$E = 341.8 - 3.51 T + 0.005641 T^2 \quad (1.19)$$

where T is the test temperature in ($^{\circ}\text{F}$) and E is in (ksi). From their test results it was argued that the relation between the absorbed energy per unit area in the Charpy tests (R) and the crack driving force (G_i) could be written as:

$$G_i = 0.055 R^2 \quad (1.20)$$

where G_i and R are in (in-lbs in^{-2}). If Eqs. 1.18 and 1.20 are combined to define the safe allowable pressure, this gives

$$P_c = \frac{0.264}{(SDR - 1)} \sqrt{\frac{E R^2}{(1 - \nu^2) a}} \quad (1.21)$$

The BCD model was criticised in the study of SwRI (Kanninen *et al.*, 1987) which pointed out that it is only valid for a short crack where $h \ll a \ll D$, it, therefore, cannot be used to interpret the propagation/arrest event where the crack lengths could be well outside this range. Furthermore, the way the dynamic modulus of a PE material is calculated was criticised. The lack of sound foundation and justification of Eq. 1.20 was also called to attention and criticised by Williams (1987) and Kanninen *et al.* (1987).

1.7.5.2. Southwest Research Institute (SwRI) Model

Taking into account the fracture mechanics principles in a fluid-structure FE analysis model, SwRI has proposed a design procedure. It was suggested that with this procedure, the minimum Charpy energy value, $(C_v)_{min}$, of the PE pipe material required to preclude RCP could be calculated. This is given as:

$$(C_v)_{min} = 0.175 \frac{D p_l^2}{E_s} (SDR - 1)^2 \quad (1.22)$$

where E_s is the static elastic modulus of the material.

$$p_c = \frac{2.4}{(SDR - 1)} \sqrt{\frac{E_s (C_v)_{min}}{D}} \quad (1.23)$$

SwRI has also argued the existence of a maximum crack driving force as in the case of gas transmission pipelines (Kanninen *et al.*, 1990).

1.7.6. Ductile Crack Propagation Models

A point approaches at which the multitude of more complex and detailed models for *ductile* pipe fracture offer little further insight on the brittle fracture problem. Ten generic ductile crack propagation models have recently been subjected by Cardinal *et al.*, (1987) to a critical comparative survey on twelve criteria:

- 1) independence of empirically determined parameters,
- 2) ability to predict crack velocity,
- 3) identification of a defined crack resistance parameter,
- 4) inclusion of gas pressure work,
- 5) pipe wall inertia,
- 6) decompression ahead of the crack tip,
- 7) pressure decay behind the crack tip,
- 8) plastic deformation of the pipe flaps,
- 9) biaxial shell straining of the pipe wall ahead of the crack tip,
- 10) wall thinning,
- 11) wall plate bending, and
- 12) external constraint (*e.g.* backfill) effects.

The general approach of many investigators has been, on the basis of experimental observations, to assume steady-state propagation with large scale inelastic dynamic material

deformation. Within some defined control volume moving with the crack tip, an energy balance is computed (accounting for some selection of the items above), as for the simple brittle fracture model; the ductile case is, however, profoundly different in that inelastic deformation terms *dominate* dissipation. Most of the crack driving work is done by the gas as it expands against the plastically-deforming pipe 'flaps', and most of this is dissipated by near-tip axial stretching; thus the gas dynamics model is also more closely coupled to pipe wall behaviour than is likely to be the case for brittle fracture. The necessary modifications have been made to the split-ring model by Emery *et al.* (1986), for example, to recover good correlation with test data for both pressure and crack opening history.

The Southwest Research Institute team responsible for this survey of ductile crack propagation models (Kanninen *et al.*, 1989) are themselves now in the fifth year of a major program to develop an improved model. Their object is to develop a small-scale test. This emphasises the point that each link in the chain, from field trial through small scale tests to material test, must be securely and consistently analysed for any fracture test to be fairly representative of service conditions.

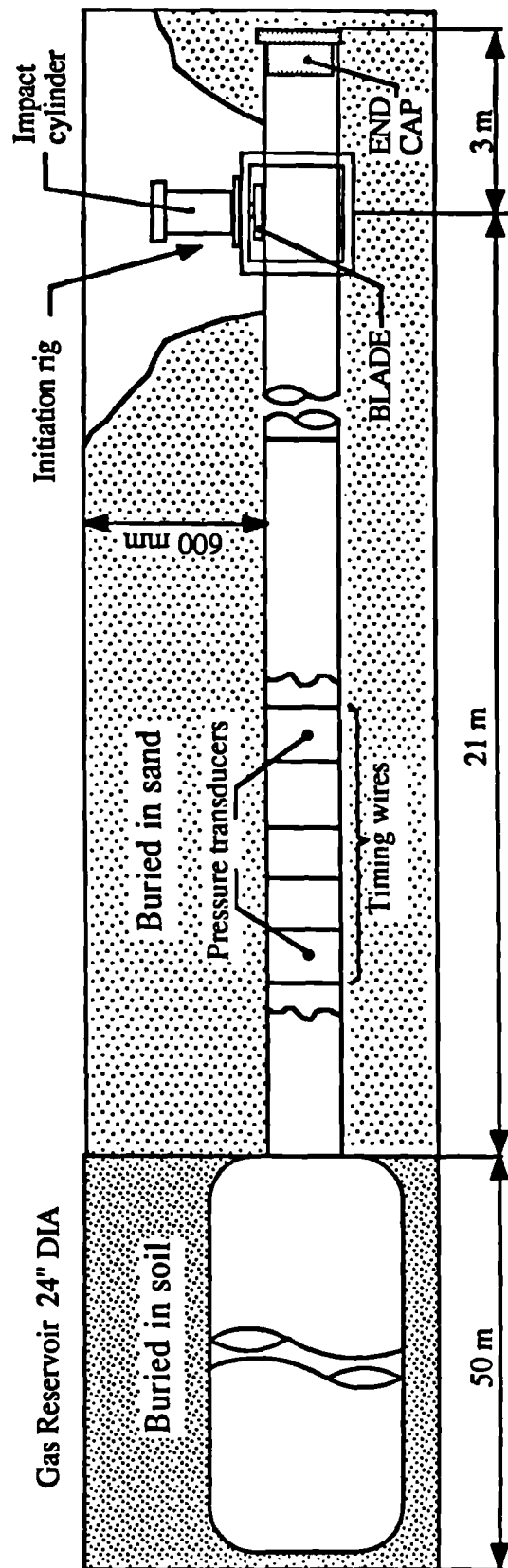


Figure 1.1 Full-scale crack propagation test facility used by BG (Greig & Ewing, 1982)



Figure 1.2 Rapid crack propagation in a full-scale test; BP 002-40 250 mm SDR 11 pipe at 0°C. Note sinusoidal weaving of the crack path.

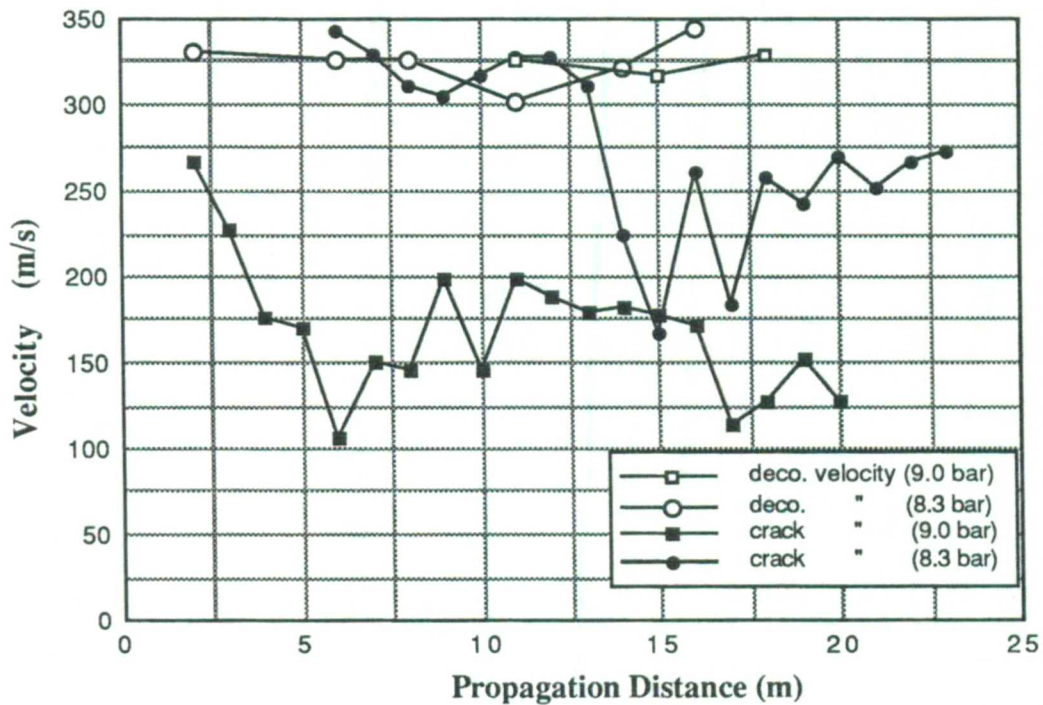


Figure 1.3 Full-scale test results showing the variation of crack velocity and decompression speed at 0 °C temperature for BP 002-40 250 mm SDR 17.6 polyethylene pipe for the pressures of 8.3 and 9 bar, (Greig, 1986).

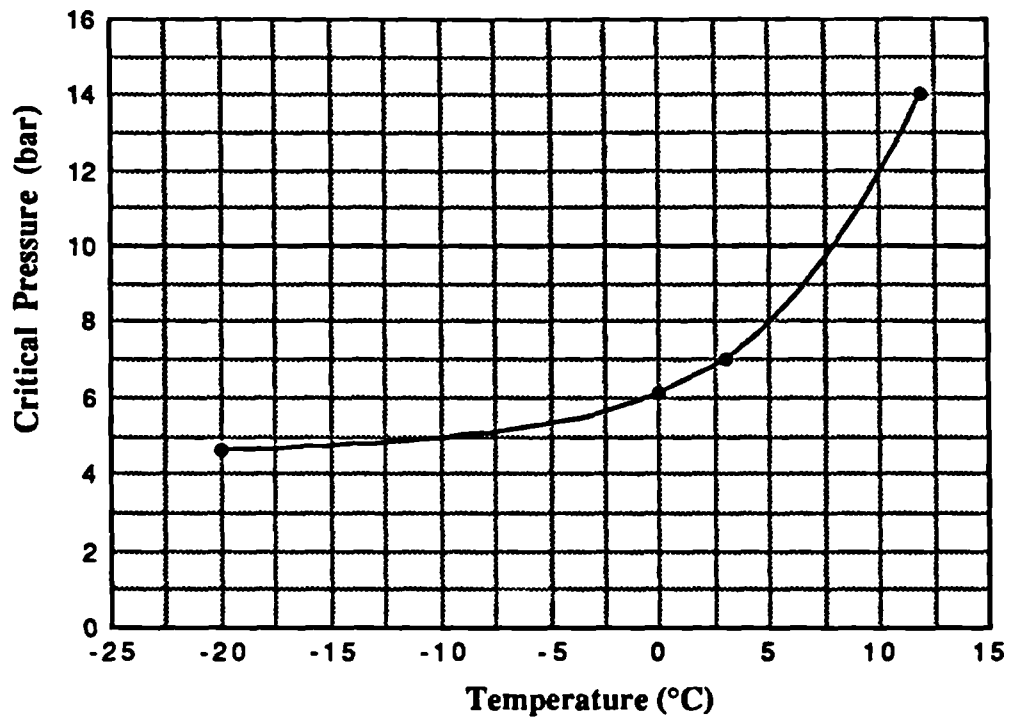


Figure 1.4 Full-scale test results showing the variation of critical pressure with temperature for BP 002-40 250 mm SDR 17.6 polyethylene pipe.

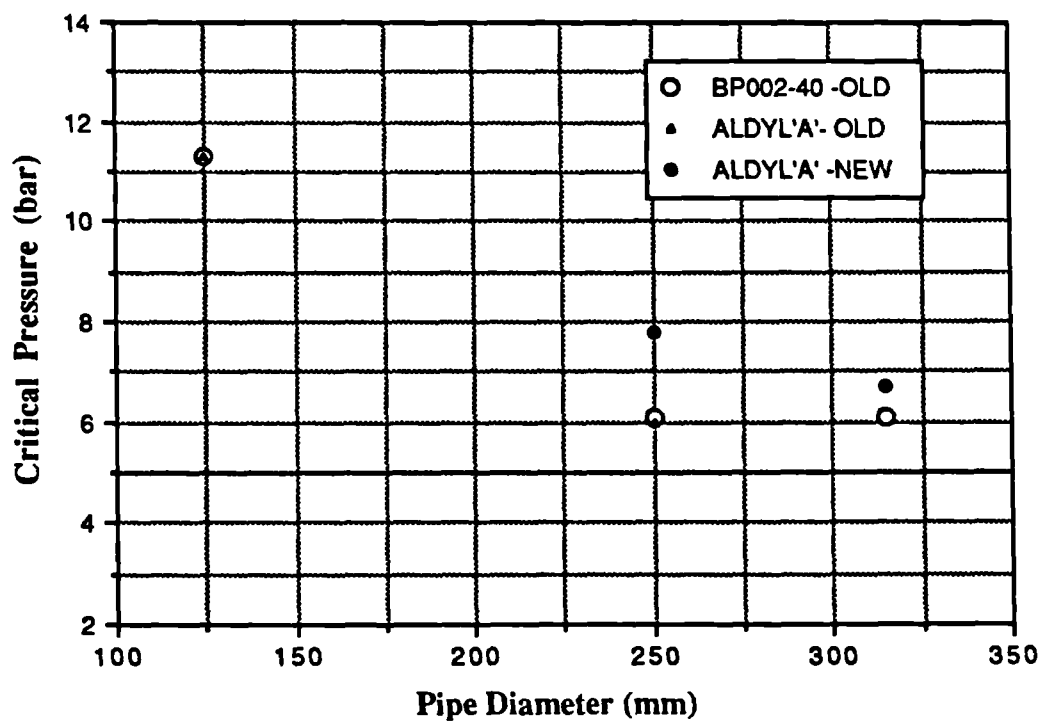


Figure 1.5 Full-scale test results showing the variation of critical pressure with diameter for SDR 17.6 PE pipes at 0 °C.

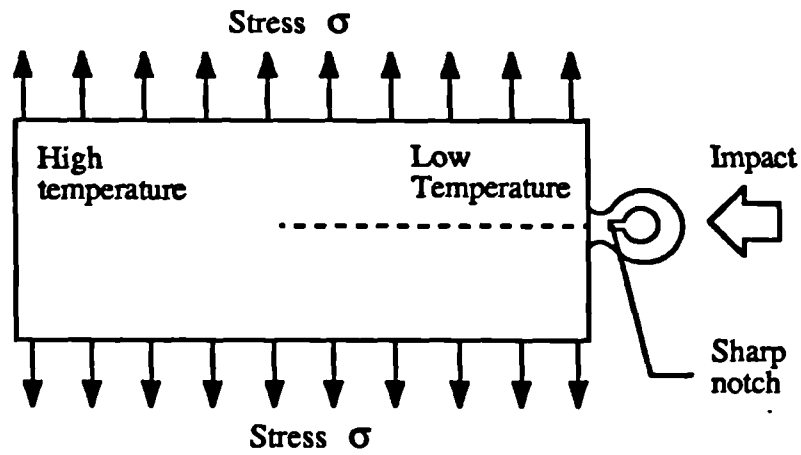


Figure 1.6 Robertson test specimen and its loading.

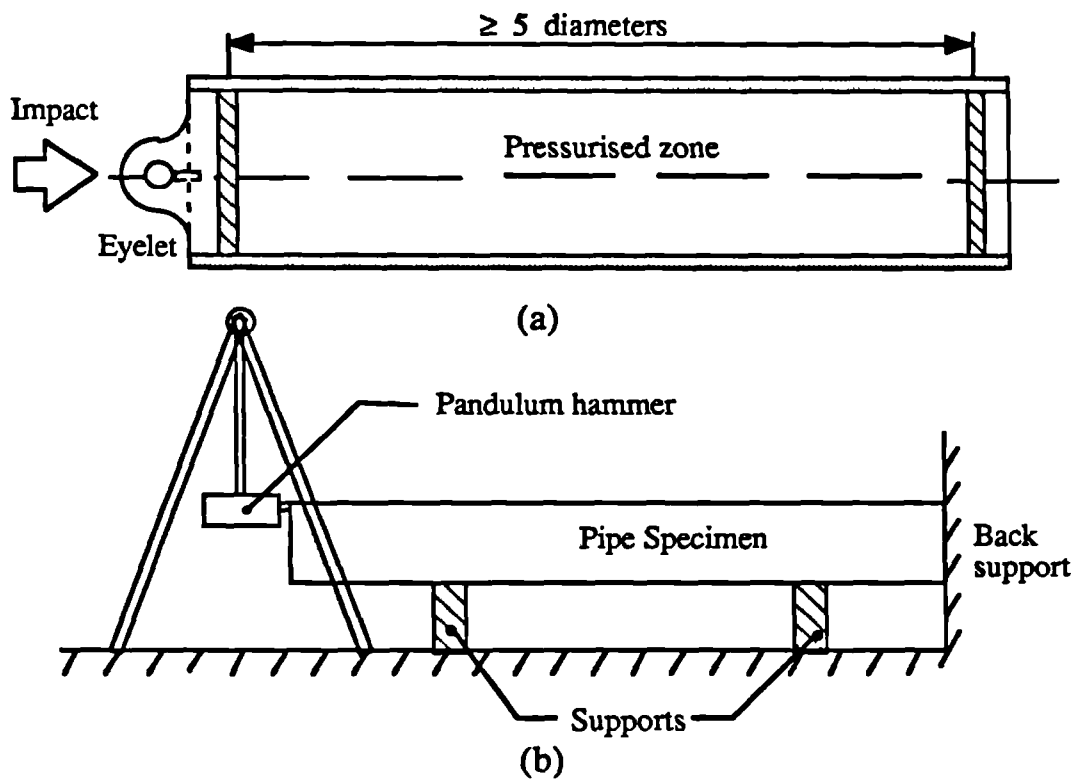


Figure 1.7 Modified Robertson test, a) Pipe specimen and notch configuration,

b) Test set-up.

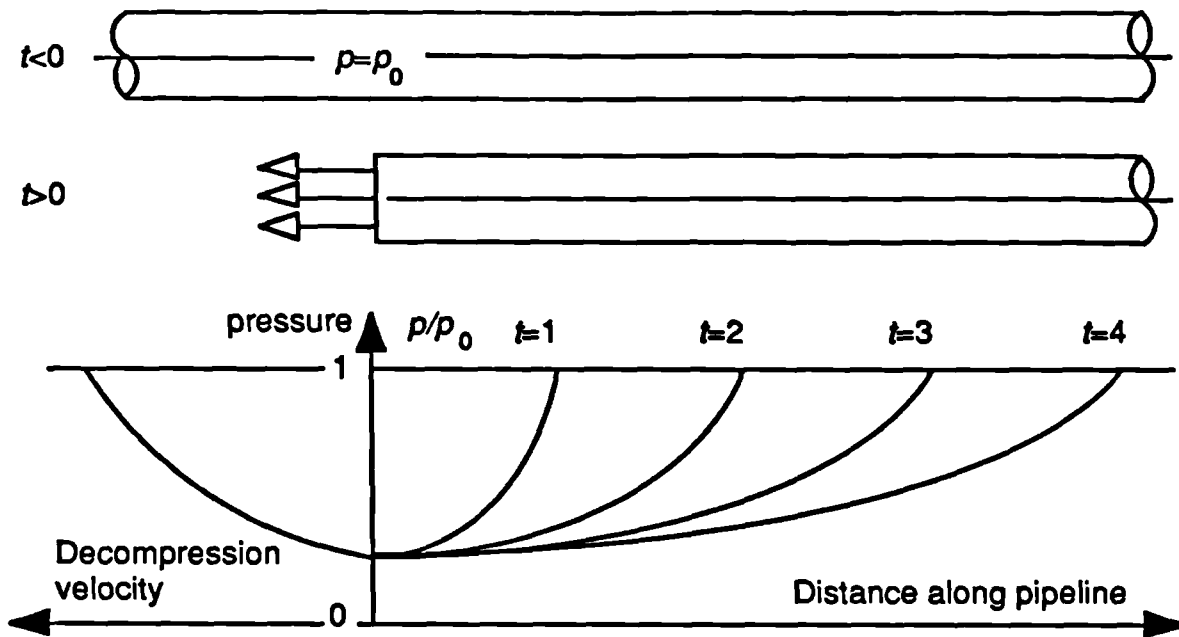


Figure 1.8 The guillotine-cut model of pipe decomposition. Pressure at the cut drops to a minimum of 28% of the initial line pressure, after Wölter & Ketel (1983).

CHAPTER 2

GAS DYNAMICS OF RAPID CRACK PROPAGATION

2.1. Introduction

In the fast fracture of gas pipelines the crack driving force is provided by the gas pressure acting on the flaps behind the crack tip and the strain energy stored in the pipe wall. Thus, for a certain material and pipe dimension whether an initiated fast crack will propagate or stop strongly depends on the pressure at and behind the crack tip. The investigation of the gas dynamics, therefore is of fundamental importance.

It will be shown in following sections that the crack speed in PE pipes varies between 70-350 ms⁻¹. If the crack speed is equal to or higher than the decompression speed of the internal gas, the crack will propagate continually to the end of the pipe without the pressure at its tip decreasing. If the crack speed is lower, the pressure at the crack tip will decrease, the energy available to drive the crack will be reduced and the crack may arrest or propagate if the driving force remains sufficiently high.

For a gas-filled pipe of infinite length, there is a finite pressure, 28% of the initial pressure, at the crack tip, even if the crack stops as shown by Eiber (1969). This brings us to the fact that, whatever the crack speed is, crack propagation continue, if 28% of the total pressure is high enough to drive the crack, if not crack arrest takes place.

The pressure starts to fall, as soon as fracture initiates. The pressure decays in the pipe, from the initial pressure, p_0 , towards the atmospheric pressure, p_a . This pressure drop is schematically represented in Fig. 2.1. The pressure decay at any section during rapid crack propagation in gas pipeline is due to two distinct processes. These are:

i) The 'discharge process' (Alder, 1974). This mode starts when the crack tip reaches the point of observation and continues after it has passed. During this process the pressure drops at a relatively higher rate as the gas expands both axially and radially into the atmosphere behind the crack tip, and finally drops to the atmospheric pressure.

ii) The 'decompression process' (Alder, 1974). Since decompression speeds are greater than crack speeds (in general) this stage always takes place in front of the crack tip as shown in Fig. 2.1. Here the initial rarefaction wave in gas proceeds along the pipe at the speed of sound in an undisturbed gas. Behind it, the pressure drops rapidly and, after a certain time or crack propagation length, which depends upon the crack velocity, decreases asymptotically to approach to a certain value which is shown by t_n in Fig. 2.1.

The nature of the flow process for (i) is three-dimensional, but for (ii) it is one-dimensional. These two processes define the value of the pressure acting on the flaps and the magnitude of crack driving force.

Although a lot of work has been done on the gas dynamics aspect of rapid crack propagating (RCP) in steel gas pipelines, the subject is little explored for plastic gas pipelines. Since for both cases the crack is partly driven and controlled by gas acting on the flaps behind the crack tip it is worth reviewing the literature. The models which have been proposed so far to describe the decompression and discharge processes will be summarised.

From the gas dynamics point of view, brittle crack propagation in steel gas pipelines is quite different from the rapid crack propagation in plastic, especially PE, gas pipelines. For the former, the crack travels at speeds in excess of the speed of sound in the undisturbed gas, c_0 . As a result, the pressure at the crack tip, p_p , is always equal to the line pressure, p_0 . For the latter, since the crack propagates at a speed lower than that of decompression, like ductile crack propagation in gas pipelines, the pressure at the crack tip is always smaller than the line pressure.

2.2. Pressure at the Crack Tip

As shown in Fig. 2.1, during the decompression process the flow inside the pipe is unsteady, varying at a certain point with time. To study decompression ahead of the crack tip, flow is assumed to be geometrically one dimensional, the gas to be perfect and the process isentropic.

The decompression process was first studied by Battelle Memorial Institute (BMI) investigators (Eiber, 1969). In their model, as indicated in Fig. 2.2, a semi-infinite circular cylindrical pipe has its end closed by an end cap and the pipe contains a perfect gas, initially at p_0 . The propagation of a fast crack travelling at a speed v_c can then be simulated by sudden removal of the end cap and simultaneous shortening of the pipe length from its end at a constant speed of v_c .

As the end cap is opened, the pressure drops immediately by a large amount. The large amplitude rarefaction (decompression) wave, which propagates in the pipe to reduce the gas pressure, can be represented as a series of small amplitude rarefaction waves as shown in Fig. 2.3. In this process the decompression front travels at a speed of sound in the undisturbed gas, c_0 , which is given by

$$c_0 = \sqrt{\gamma RT} \quad (2.1)$$

where γ is the specific heat ratio of the gas, R is the gas constant and has a value which depends on the chemical composition of the gas and its molecular weight. T is the temperature. The specific heat ratio γ can also be written as

$$\gamma = \frac{R + c_v}{c_v} \quad (2.2)$$

where c_v is known as the specific heat of the gas at constant volume. The typical values for c_v , R and γ of various ideal gasses are listed in Table 2.1 (Cravalho & Smith, 1981).

Table 2.1 The typical values for c_v , R and γ of various ideal gasses

Gas	Composition	Molecular Weight	R (kJ/kg K)	c_v (kJ/ kg K)	γ
Air		28.97	0.287	0.716	1.401
Carbon Dioxide	CO ₂	44.01	0.189	0.662	1.285
Helium	He	4.00	2.078	3.153	1.659
Hydrogen	H ₂	2.02	4.116	10.216	1.403
Methane	CH ₄	16.04	0.518	1.726	1.300
Nitrogen	N ₂	28.01	0.297	0.741	1.401
Oxygen	O ₂	32.00	0.260	0.657	1.396

From Eqs. 2.1 and 2.2 the speed of decompression front can be calculated. It seems adequate to regard natural gas mixtures as an ideal gas composed largely of methane. Thus, at 0 °C the speed of decompression front in a natural gas pipe is around 428 ms⁻¹ which is, in almost all the cases, above the crack speed. For the same temperature, the value for air pressurised pipe is about 331 ms⁻¹. As pointed out by Abbassian (1985) the gas behind this wave is now at slightly lower pressure but occupies a slightly larger volume than the undisturbed gas, and it flows towards the open end of the pipe at a uniform velocity. Another small rarefaction wave moves through this gas at a slightly lower speed than c_0 relative to the gas, which is itself moving backwards as a rigid body. As this idea is extended for the whole process, it can be understood that each component of the rarefaction wave travels at the local speed of sound relative to the gas in which it propagates.

Thus, each rarefaction wave travels with an absolute velocity v_w given by

$$v_w = c - u \quad (2.3)$$

where c is the local velocity of sound in the gas and u is the local velocity of gas in the pipe. As each wave propagates towards the open end the local velocity of gas, u , increases and consequentially approaches the local sonic speed, c . After a certain time the flow process

eventually becomes choked at the crack tip, *i.e.* the Mach number, M , which is the ratio of local velocity of the gas (u) to the local velocity of sound in the gas (c) takes the value of 1. Consequently, further rarefaction waves cannot get into the pipe because

$$v_w = c - u = 0 \quad (2.4)$$

thus, the gas pressure at the crack tip settles to a constant value and the flow process inside the pipe becomes steady.

Unsteady one-dimensional simple wave theory of a perfect gas, details of which are given by Shapiro (1953), Rudniger (1955) produces an expression relating the absolute wave speed, v_w , to the gas pressure in which the wave is propagating, p :

$$\frac{p}{p_o} = \left[\frac{2}{\gamma + 1} - \frac{\gamma - 1}{\gamma + 1} \left(\frac{v_w}{c_o} \right) \right]^{\frac{2\gamma}{\gamma - 1}} \quad (2.5)$$

Now, it is worth considering what happens if the pipe end moves to the right, parallel with a rarefaction wave as shown in Fig. 2.2. For this case the gas pressure just upstream of this rarefaction wave is the pressure which would be measured by an observer moving with the moving end. Extending this idea for a crack propagating with a speed v_c , Eiber (1969) substituted $v_w = v_c$ and $p = p_t$ at the moving crack and has shown that the following equation may be derived for the nominal crack tip pressure:

$$p_t = p_o \left[1 - \frac{\gamma - 1}{\gamma + 1} \left(1 - \frac{v_c}{c_o} \right) \right]^{\frac{2\gamma}{\gamma - 1}} \quad (2.6)$$

Eq. 2.6 can be applied to obtain the pressure at the crack tip of a propagating crack which is propagating at a constant velocity v_c . The equation is plotted in Fig. 2.4 which shows that for a crack travelling at a typical speed $0.5 c_o$ in an air pressurised pipe ($\gamma=1.4$) the pressure at the crack tip would be $\approx 0.55 p_o$.

Eiber (1969) and Maxey (1974) have shown that there is a satisfactory agreement between the prediction of Eq. 2.6 and the experimentally measured values. They also observed that for a propagating crack to attain steady-state propagation the crack has to travel at least 10 m from the initiation. Thus Eq. 2.6 gives satisfactory results after the crack speed attains this steady-state condition.

2.3. Pressure Distribution Behind the Crack Tip

The pressure decay during the discharge process taking place behind the crack tip in which the pressure decays from the crack tip value which is predicted in Section 2.2 to the atmospheric pressure is primarily due to two separate components. These are firstly the escape of gas from the cracked pipe to the surroundings and secondly the expansion of the gas in the distorted flap region.

It was shown in Section 2.2 that a theoretical expression for decompressed pressure value at the crack tip can be obtained and depends on the crack propagation velocity and the properties of the gas, *i.e.* γ and c_0 . However the derivation of a theoretical expression for the pressure decay behind the crack tip is far more complex due to the complicated three-dimensional geometry of flaps and nature of the flow in that region.

For modelling the discharge process, it would be appropriate to regard the pressure decay behind the crack tip as a constant pressure-distance profile relative to a frame of reference moving with the crack tip, the discharge phase becomes steady flow of the gas through a stationary crack, *i.e.* the discharge flow has a fixed pattern when viewed from the moving crack tip.

The gas discharge process behind the crack tip is a three dimensional unsteady state in nature. A proper three dimensional analysis of the discharge flow for a given shape of flaps requires heavy computations. As a result of this, in order to study this complex three-dimensional unsteady state flow process analytically a couple of simplified models have been developed so far. These can be classified as 'one-dimensional models' and 'two dimensional models'.

2.3.1. One Dimensional Models

These models are based upon the theory of the one dimensional flow of ideal gases with constant specific heats. The mean cross sectional pressure is predicted as a function of distance from the crack tip. The one dimensional models assume that the axial flow and the leakage through the cracked section on the top of the pipe behind the crack tip are uncoupled and can be treated separately. The second assumption in these one-dimensional models is that crack opening is very small compared to the pipe diameter. Although this is hardly the case for ductile crack propagation in steel pipelines where the crack openings are of the order of the pipe diameter (even sometimes a flattened shape have been observed) it is generally the case for RCP in PE or other plastic pipelines (such as PVC).

Using the isentropic relationship, as given by Shapiro (1953) for a perfect gas, and using the Mach number, M , as the dependent variable which determines the gas properties at the cross section x , the following relationship between M and crack geometrical parameters can be written:

$$\frac{dM}{dx} = \frac{1}{A} \left(M \frac{dA}{dx} + k W \right) \left\{ \frac{1 + \frac{\gamma - 1}{2} M^2}{M^2 - 1} \right\} \quad (2.7)$$

where $A=A(x)$ is the cross sectional area of the pipe and $W=W(x)$ is the crack opening of the section x as shown in Fig. 2.5 and k is a constant value depending upon the gas properties and is given by

$$k = \left[\frac{2}{\gamma + 1} \right]^{\frac{\gamma + 1}{2(\gamma - 1)}} \cong 0.58 \text{ for air } (\gamma = 1.4)$$

Using the boundary condition $M=1$ at $x=0$ (choked flow at the crack tip) Eq. 2.7 can be solved to obtain the variation of M behind the crack tip. When M is known, the gas pressure behind the crack tip can be obtain from isentropic relations:

$$p = p_t \left[\frac{\gamma + 1}{2 + (\gamma - 1) M^2} \right]^{\frac{\gamma}{(\gamma - 1)}} \quad (2.8)$$

Eq. 2.7 and thus Eq. 2.8 can be solved if W , A and dA/dx can be expressed as functions of x . To define these geometrical variables two approaches were followed.

- i) Assume an appropriate pipe distortion so W and A can be expressed as functions of x . This method was used by Alder (1974).
- ii) Solve Eq. 2.8 simultaneously with governing pipe deformation equations. This method was used by Kanninen *et al* (1976).

2.3.1.1. Alder's One-Dimensional Model

The discharge of gas into the atmosphere through a propagating crack has been modelled by Alder (1974) at the University of Edinburgh. He has shown that the mean cross sectional pressure is a single function of the axial distance from the crack tip and the angle between the edges. He showed that for a crack geometry as shown in Fig. 2.5, the solutions to the Eq. 2.7 with and without area change, do not differ significantly. By assuming that $dA/dx=0$, he solved Eq. 2.7 analytically and obtained M as a function of x . He further indicated that his theoretically obtained pressure distribution can be fitted by the following exponential expression.

$$p = p_t \exp \left\{ - (0.73 + 0.70 \gamma) \left[\frac{k}{d^2} \int_0^x W dx \right]^{\frac{1}{2}} \right\} \quad (2.9)$$

in the above equation the value of $\int W dx$ denotes the crack opening area in plan view up to the x (crack tip) and is represented by A_p in Fig. 2.6.

Eq. 2.9 is plotted in Fig. 2.7 for $\gamma=1.4$ and 1.3, for air and methane, respectively. In this figure p is normalized with respect to the crack tip pressure, p_t , and A_p is normalized with

respect to the pipe cross sectional area, $A_o = \pi d^4/4$. As can be seen from the graph, the pressure drops to about 80% of its crack tip value at a section where $A_p = 0.20 A_o$.

The pressure distributions on the inner surface of different pipe models with particular shapes and crack angles were measured. In these experiments the geometry used was similar to that shown in Fig. 2.5. The models were constructed from brass tube having an internal diameter of 12.7 mm and a wall thickness of 0.76 mm. The tests were performed on four crack models having crack angles of 24.0°, 28.8°, 44.6° and 62.0°. Pressure tappings were installed in the walls of the models through which compressed air at 11 bar was discharged from a large reservoir. In these experiments the average pressure at tappings 90° from the edge of the crack, *i.e.* at points A and A' in Fig. 2.6, agreed closely with the values predicted from Eq. 2.9.

If the edges of the crack are assumed to be straight, as shown in Fig. 2.6, then the crack opening area $A_p = x^2 \tan \theta$ and Eq. 2.9 can be rewritten as

$$p = p_i \exp \left\{ - (0.73 + 0.70 \gamma) [k \tan \theta]^{\frac{1}{2}} \frac{x}{d} \right\} \quad (2.10)$$

As can be seen from Eq. 2.10 for a certain gas and pipe diameter this theory predicts that the axial pressure distribution along the straight-edged crack is a single function of crack angle, θ and the distance x . The larger the crack angle the faster the decay. For $\gamma=1.4$ (air) and for a half crack angles $\theta=12^\circ$ and 22° , Eq. 2.7 is plotted in Fig. 2.8. It is worth pointing out that for this range of θ the pressure decays to about 15-30% of its crack tip pressure at a distance two diameters behind the crack tip.

More recently, it has been pointed out by Alder (1989) that his model could predict a sensible pressure history profile behind the crack tip if the initial gas pressure is at least 10 times the ambient pressure outside the pipe.

2.3.1.2. Kanninen's One Dimensional Model

Using the coupled 2-D shell deformation equations and 1-D gas dynamic equation simultaneously using a finite difference method, Kanninen *et al* (1976) predicted the pressure decay behind the crack tip. For their shell deformation model, they used the elastic small-deflection equations of circular cylindrical shells and assumed that there is no axial displacement, each cross section deforms in its original plane. It will be shown in following chapters that this is hardly the case, in that there is a certain amount of axial displacement in the deformation process.

2.3.2. Two-Dimensional Models for Pressure Decay

Full scale test results of Ives *et al.* (1974) on gas pressurised steel pipeline have shown that crack opening profiles flare out very rapidly as compared to the idealisation of a relatively long and narrow slit. Crack openings of the order of one pipe diameter were observed, violating the assumption that crack opening is much smaller than the pipe diameter. It has to be noted that for RCP in plastic pipelines, such as PE, crack openings are not as high as those in steel pipelines.

Alder (1977) and Parks & Freund (1976) pointed out that good agreement between Alder's theoretical prediction and experimental results may be fortuitous. The idealised crack geometry assumed by these authors is shown in Fig. 2.9: the pipeline has a square cross-section with the top fully open to represent the deformed pipe behind the crack tip. This assumption made it possible to model the problem in terms of two-dimensional planar flow between two parallel plates. The plate at the top of the 'trench' identifies the crack tip, while the other vertical planes representing the sides of broken pipe remain. The vertical and horizontal distances between the planes are equal to the pipe diameter, d . The boundary conditions at the crack exit plane are the same as those taken into account in one-dimensional model: flow is sonic, i.e. $M=1$, and the pressure is given by Eq. 2.6. The gas expands supersonically downstream of the exit plane to the atmosphere.

In this two-dimensional model the gas is assumed perfect, flow is steady and isentropic, and the equation of motion can be solved using the numerical method of characteristics [see, for example, Thompson (1972)]. This solution indicates the presence of a curved shock wave in the flow region, as shown schematically in Fig. 2.10.

From this two-dimensional model, the constant pressure contours, *i.e.* isobars, in the region, obtained by Alder (1977) for air ($\gamma=1.4$) and methane ($\gamma=1.3$) are shown in Fig. 2.11 and indicate that pressure drops along the x-direction and increases in the y-direction. Thus, the pressure decay is faster in regions near the crack path and slower nearer the bottom of the pipe, but these differences diminish as the distance downstream (x/d) increases. It is worth mentioning that the model predicts the average pressure acting on the flaps drops to 20% of crack tip pressure within two diameter distance behind the crack tip which was confirmed by full-scale tests on steel pipes carried out by Ives *et al* (1974) as well as by full-scale tests on PE pipes carried out by Greig (1983).

Another two-dimensional model has been proposed and is being used by SwRI in their uncoupled version of the FE code to define critical pressure. This model defines the pressure at the crack tip using Eq. 2.6, but evaluates the pressure profile behind the crack tip which varies circumferentially, as an exponentially decaying fourier series:

$$p(x, \alpha) = p_0 \exp\left[B_0 \frac{x}{d}\right] + p_1 \cos(\alpha) \exp\left[B_1 \frac{x}{d}\right] + p_2 \cos(2\alpha) \exp\left[B_2 \frac{x}{d}\right] \quad (2.11)$$

where P_0 , P_1 , and P_2 as well as B_0 , B_1 , and B_2 are calculated separately for each of the five values of crack speed so that $v_c=25, 100, 175, 250$, and 375 ms^{-1} using a nonlinear least square error minimisation computer program. The angle (α) is measured from the bottom of the pipe, $\alpha=\pi$ at the crack path. For comparison the pressure history behind the crack tip for different crack velocities is plotted in Fig. 2.14.

Beside the uncoupled FE code of SWRI, a coupled version of the FE code was also developed. As distinct from the uncoupled calculations, the initial pressures were set equal to the actual line pressure. For the coupled calculations, a three dimensional finite difference code FLOW3D is used to model the gas flow escaping from the pipe. In the code, after the gas dynamic calculations are performed, the gas pressures are converted into equivalent nodal forces on the pipe wall. Fig. 2.12 shows the velocity vectors in the symmetry plane in the immediate vicinity of the crack tip for the simulation of a 300 mm pipe with a crack travelling at 166 ms^{-1} , O'Donoghue *et al.* (1989). Due to the decompression wave travelling ahead of the crack tip, the flow in that region is axial with no circumferential variation. The flow of the gas through the opening breach is similar to Alder's model, and, although three-dimensional in nature, is still primarily axial.

2.4. Circumferential Pressure Variation Behind the Crack Tip

Two dimensional models suggest that the pressure acting on the flaps varies with the distance behind the crack tip as well as around the circumference of the pipe. As discussed earlier in one-dimensional models, axial and transverse flow are treated separately so that their separate contributions to the reduction of pressure can be identified. In two-dimensional models it is assumed that full crack openings occur abruptly behind the crack tip and the gas is allowed to expand freely against the ambient pressure in a two-dimensional way.

Taking the mean pressure, p_m , at $y=d/2$, the variation of p/p_m across various cross-sections of the two-dimensional model (Fig. 2.11), is plotted in Fig. 2.13 using the experimental results of Alder (1974). He observed that, for a given crack angle, the pressure around the circumference, normalized with respect to the mean pressure, *i.e.* the pressure at points A and A' in Fig. 2.6, have more or less the same form for all cross sections behind the crack tip. From the figure it is clear that the circumferential pressure distribution depends on the value of α , and its variation becomes more marked as the angle increases. Alder (1974) proposed the following equation:

$$p = p_m \left[(1.04 - 0.032 \alpha) + (0.032 \alpha - 0.04) \left\{ 2 \left(\frac{2s}{\pi d} \right) - \left(\frac{2s}{\pi d} \right)^2 \right\} \right] \quad (2.12)$$

where s is the distance round the circumference from the edge of the crack. Thus, Eqs. 2.10 and 2.12 will together give an adequate picture of the pressure over a pipe surface with a straight edged crack.

2.5. Comparison Between Different Models

The models that have been proposed for the pressure distribution behind the crack tip have their own characteristics. The two-dimensional models discussed above are insensitive to crack characteristics, (e.g. crack velocities and crack angles). In general, of course, as pointed out by Parks & Freund (1978) the crack velocity is taken into account in calculations via Eq. 2.10.

The variation of gas pressure as predicted by the above models is compared in Fig. 2.14. The result of Alder's one-dimensional model is plotted for typical half crack angles $\alpha=14$, 35° and 55° . The results of Kanninen's one-dimensional model and Park's two dimensional model, plotted on the same graph, show that the pressure distribution varies between Alder's and SwRI's model. The pressure profiles of SWRI model for different crack speed are plotted in the same figure. The pressure profiles behind the crack tip for Kanninen's (1976) and Alder's (1974) models are shown in the same figure for typical crack speeds $v=122$, 183.3 , and 244 ms^{-1} . In these plots γ is taken to be 1.3 (methane). From these two models it seems that pressure decays more or less exponentially behind the crack tip and the rate of decay is greater for cracks travelling at lower speeds, which implies that larger crack openings are associated with slower crack speeds.

For RCP in plastic pipelines, the crack opening angles are not as high as those in steel pipelines. Consequently, Alder's one-dimensional model may give a more sensible pressure profile if the half crack opening angle is taken as 14° or less. On the other hand it is impractical to assume a specific value for this angle, since the angle depends strongly upon

material properties, (*e.g.* elastic modulus and fracture toughness) as well as pipe dimensions (*i.e.* SDR and diameter). Beside these parameters, of course, the presence of flap restraints, such as backfill, causes the pressure decay rate behind the crack tip to decrease.

2.6. Non - Ideal Conditions

In the models reviewed so far, it has been assumed that the gas behaves as a perfect gas and the flow is isentropic. These assumptions simplified the analytical solutions and made them relatively simple. However, as the gas moves to the cracked end it expands and cools to very low temperatures during decompression and discharge process. Consequently heat transfer can take place between the pipe and surroundings.

Cronje *et al* (1980) studied the effect of heat transfer on the decompression process in steel pipes. They pointed out that although heat transfer and friction tend to increase the value of the pressure at the crack tip, the crack must travel a long distance before this increase becomes significant. Since the discharge process is very rapid there is not sufficient time for the heat transfer and frictional effects to take place. For RCP in plastic pipes, the operating pressure is relatively low compared to steel pipes and thus, it is reasonable to assume that the flow is isentropic.

Another simplification in the gas dynamic models was the assumption of perfect gas which may only be true when the gas pressure is moderately low and the density is low. When the pipelines carry 'rich' gases which contain heavy hydrocarbons, depressurization and cooling of the gas may cause the hydrocarbons to condense, producing liquid droplets in the flow. In other words the gas enters a 'two-phase' regime. When the condensation process occurs at the crack tip; the shape of $p_t - v_c$ curve in Fig. 2.4 changes and takes the form of Fig. 2.15 showing a 'plateau' at the beginning of 'two-phase flow'. As the gas enters this region, its pressure, temperature, velocity and density remain unchanged but the speed of sound, c , in the gas decreases. The decompression wave speed, v_w , suddenly reduces and consequently the crack tip pressure increases by an amount which depends on the location and extent of the plateau, which in turn is defined by the gas composition and its initial pressure.

The condensation and its effect on decompression process has been studied by King (1978), Alder (1977) and Groves *et al.* (1978). King (1978) observed that the condensation process can cause a 10-20 % increase in the crack tip pressure. Their results showed that condensation could either occur ahead of the crack tip during decompression or more likely, behind the crack tip during discharge. Thus condensation may have some effect on pressure behind the crack tip as well. The results of Maxey (1979) indicated that in the case of two-phase flow, the pressure on the flaps is higher than that in the case of single phase-flow. The results of Alder (1977) show that for a decrease in γ from 1.4 to 1.3, the average pressure normalised with respect to the crack tip pressure increases by about 5% (see Fig. 2.11).

2.7. Conclusions

The measured crack speeds for RCP in PE pipes are similar to those observed in ductile crack propagation in steel pipes: 70-350 ms⁻¹. For a crack propagating at a velocity less than the speed of decompression of the gas, the pressure at the crack tip is not the line pressure, and the pressure distribution acting on the pipe wall, as well as the crack tip pressure, affect the crack driving force. Although not much research has been done on the gas dynamics side of RCP in plastic pipes, the models proposed for ductile long running cracks in steel pipes could be applied to plastic pipes, as well.

The main conclusions from these models can be summarized as:

- i) The pressure at the tip of a constant velocity crack can be accurately predicted.
- ii) For the variation of pressure behind the crack tip, various different models have been proposed so far, but they can only give a qualitative estimate of the three-dimensional complex discharge phenomenon.
- iii) Ovalisation of the pipe has little influence on either crack tip pressure or pressure acting on the flaps.

- iv) In general, although the condensation of the gas increases both crack tip pressure and the pressure acting on the flaps, this occurrence is less likely the case for the plastic pipe in that, at least for the time being, the operating pressure for the pipe is relatively low compared to that for steel pipe.

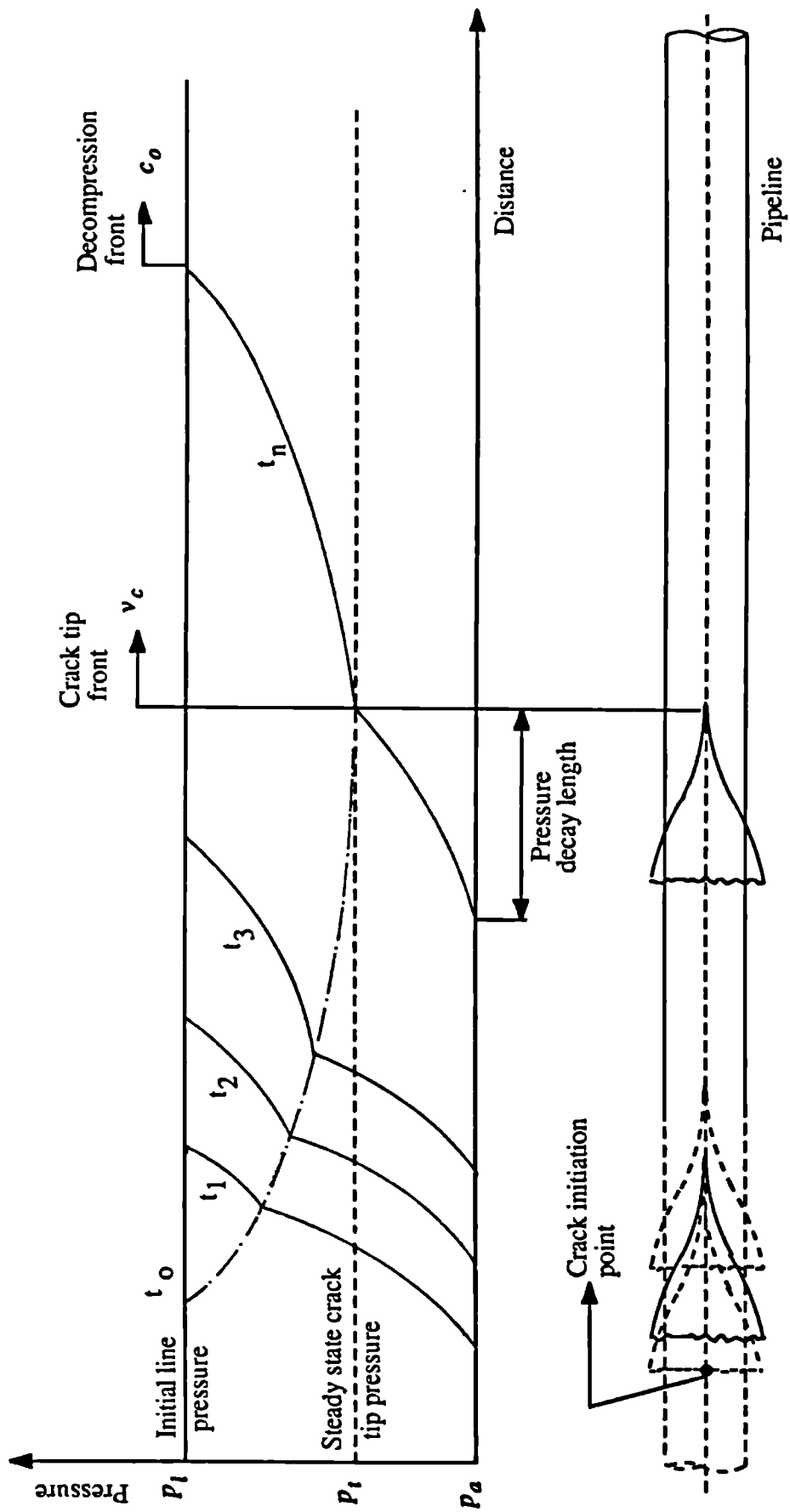


Figure 2.1 Schematic variation of pressure decay in pipeline during rapid crack propagation.

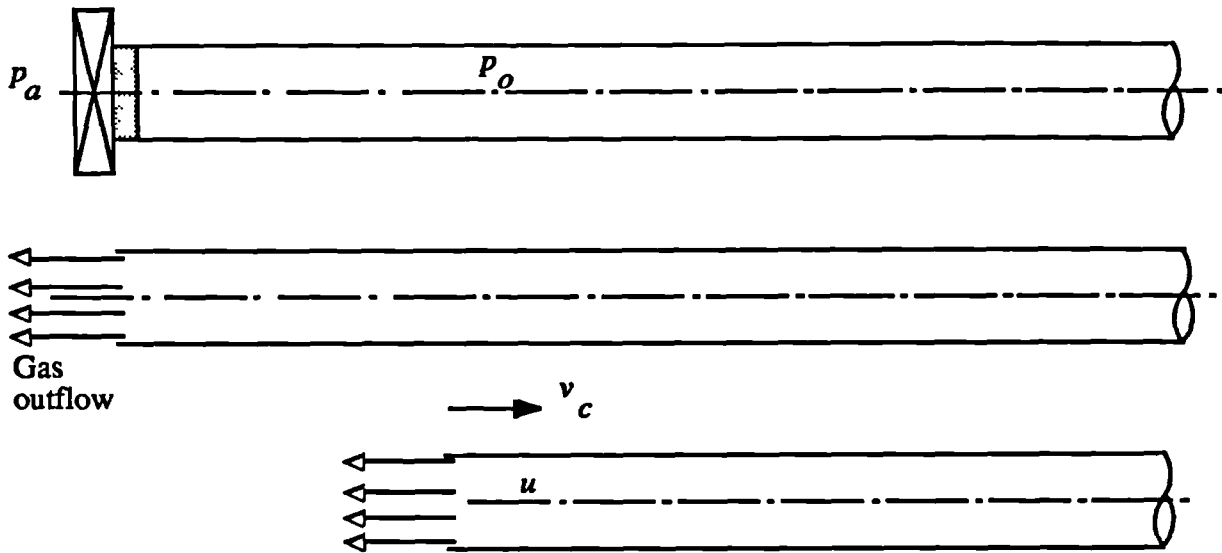


Figure 2.2 Sequence of events in the decompression process.

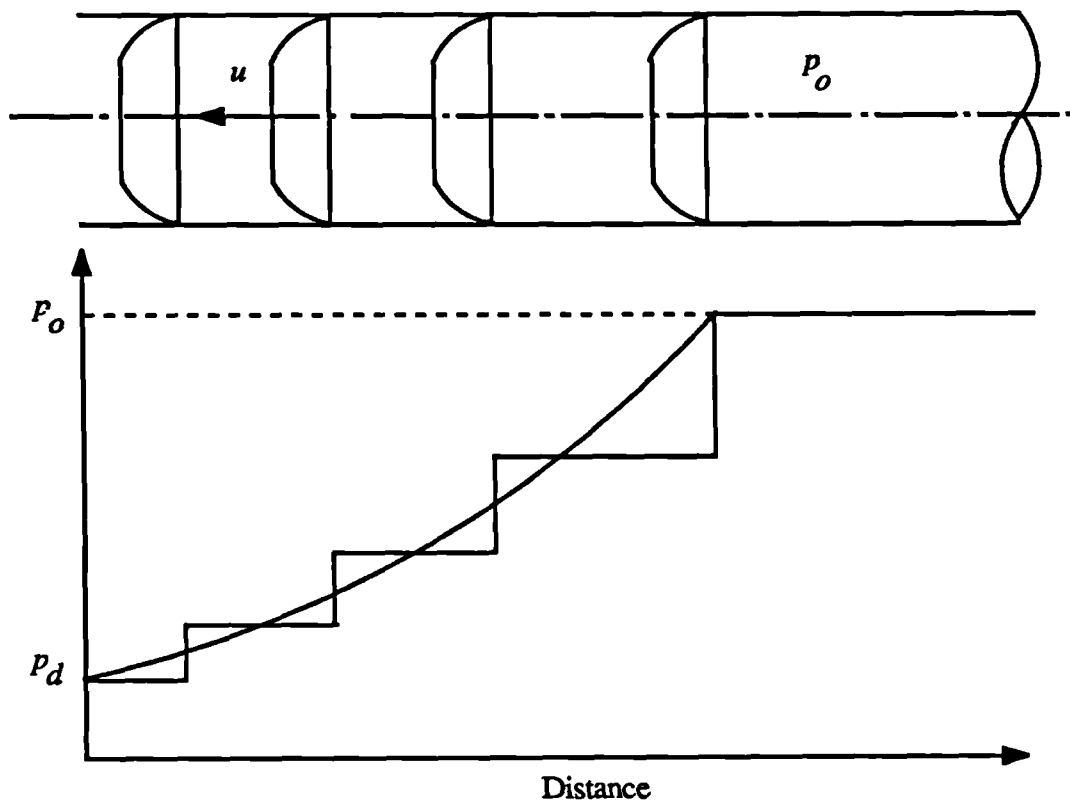


Figure 2.3 Large amplitude decompression wave and its representation as a series of small amplitude rarefactions waves (Abbassian, 1985).

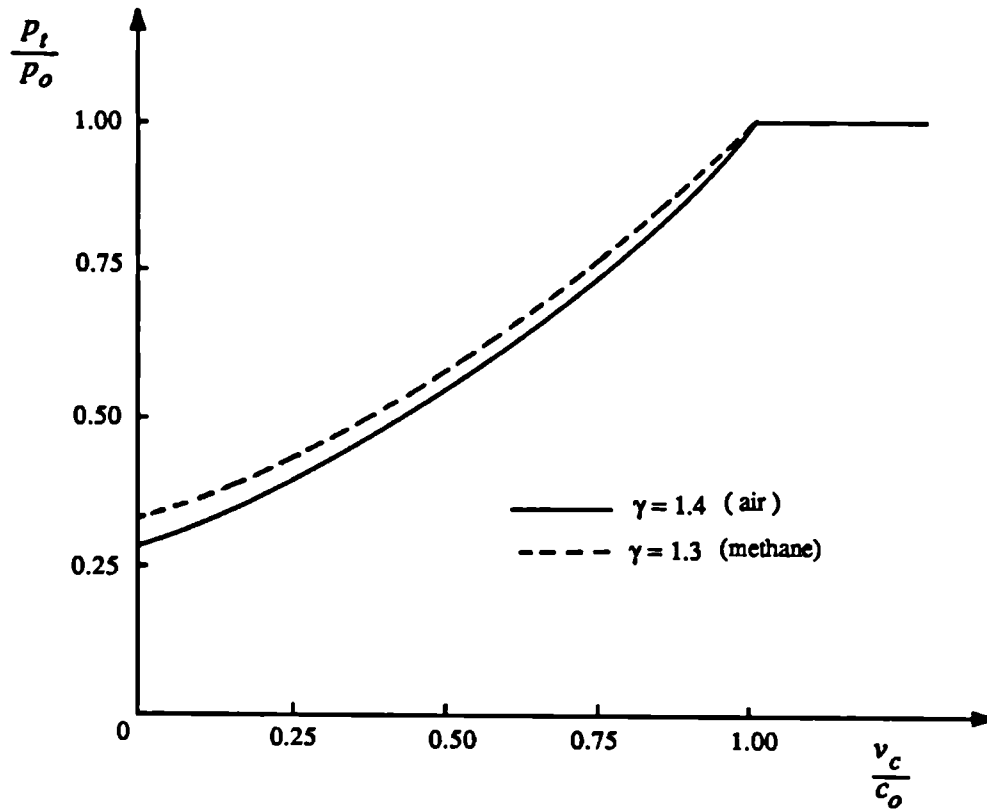


Figure 2.4 Variation of crack tip pressure with crack velocity (Eq. 2.6).

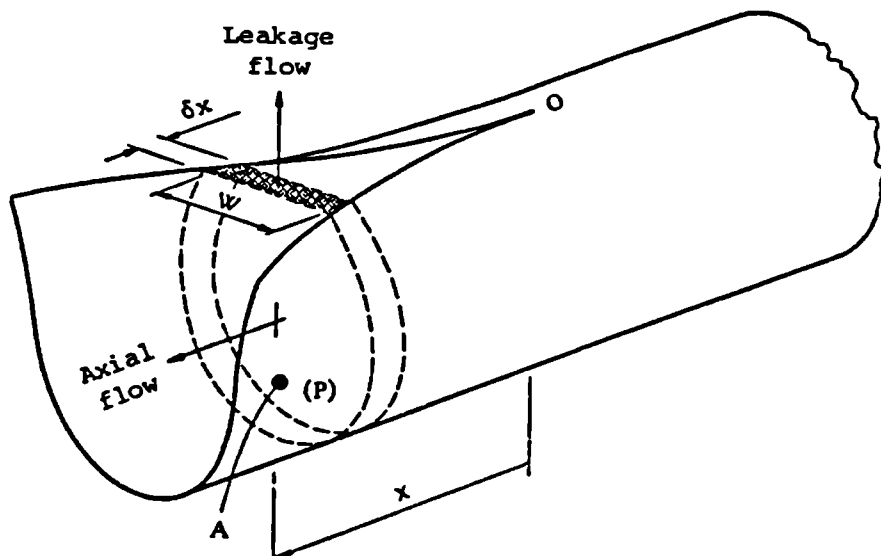


Figure 2.5 Radial and axial flow components in discharge process behind the crack tip which is assumed in one dimensional model.

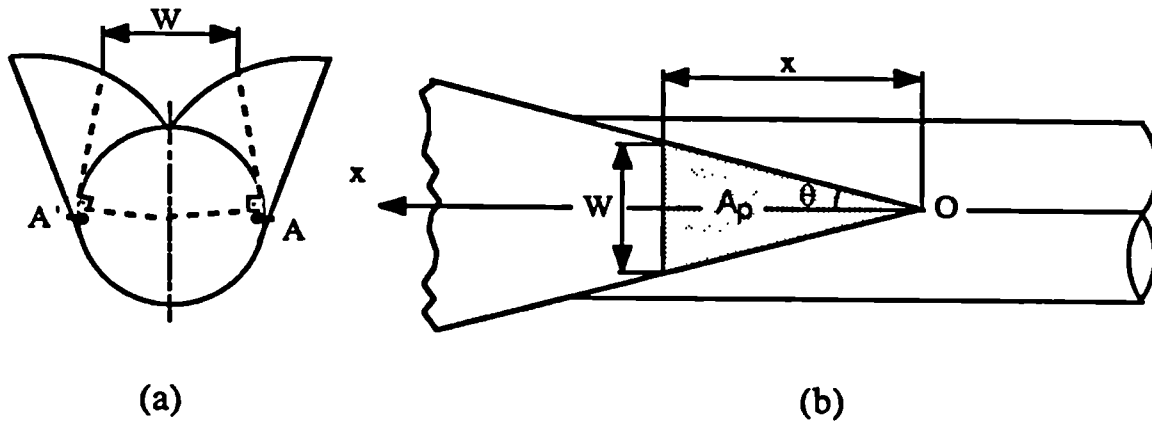


Figure 2.6 One dimensional gas dynamic model assumed by Alder (1974).

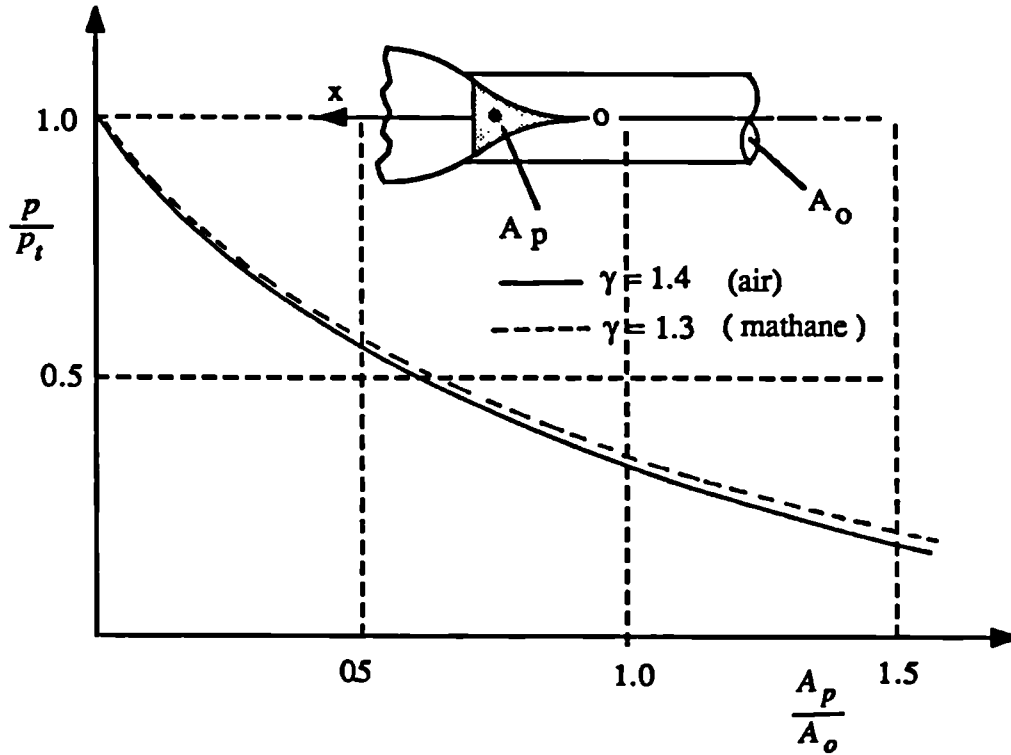


Figure 2.7 Effect of crack opening area on the pressure distribution behind the crack tip given by Eq. 2.9 (Alder, 1974).

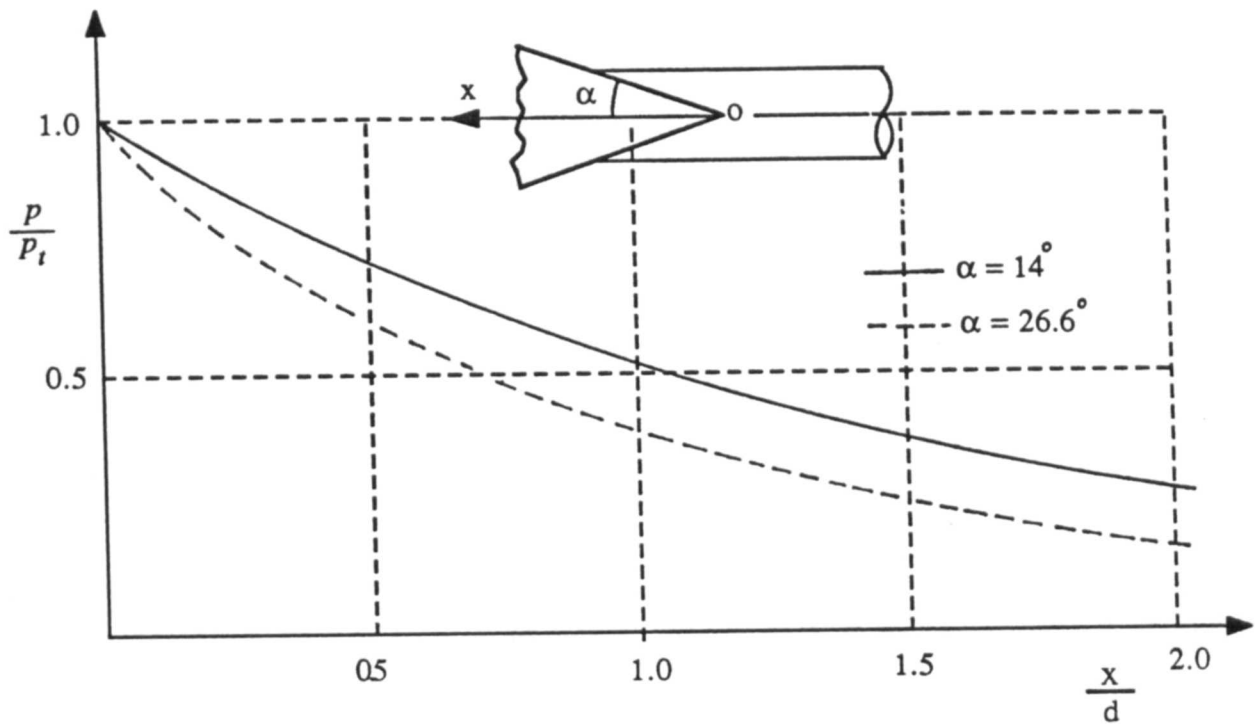


Figure 2.8 Alder's pressure distribution model behind the crack tip for different crack angles, Eq. 2.10 ($\theta = 14^\circ$ and 26.6°).

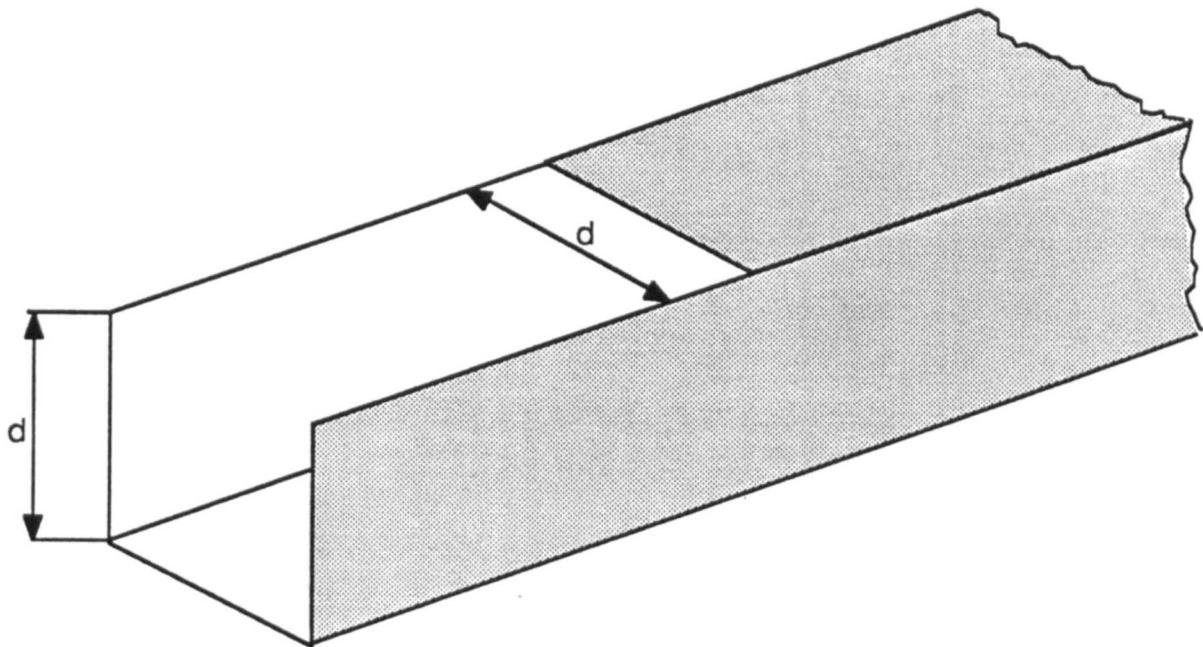


Figure 2.9 Idealised flap geometry assumed for two dimensional gas dynamic model by Parks & Freund (1978).

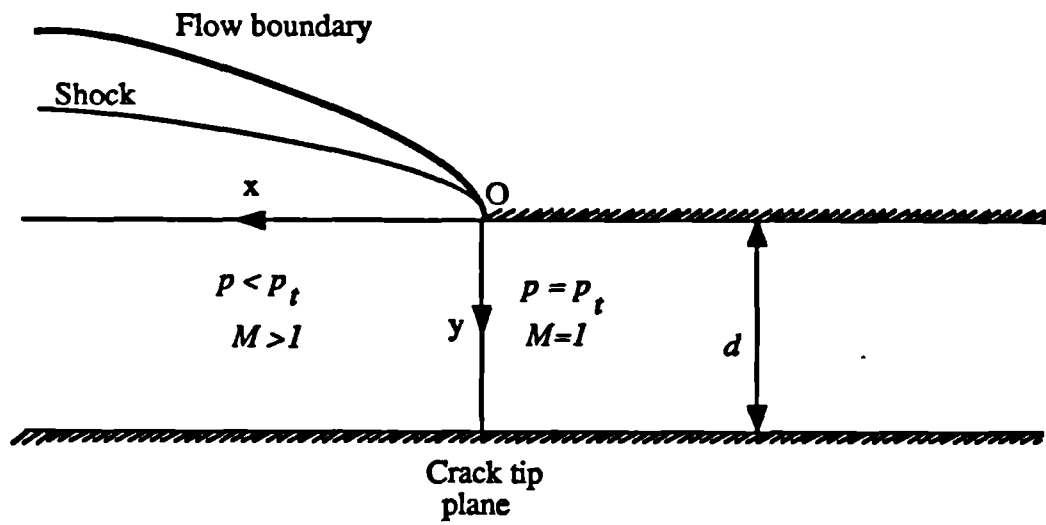


Figure 2.10 Two dimensional planar discharge flow through the idealised crack geometry of Fig. 2.9 (Parks & Freund, 1978).

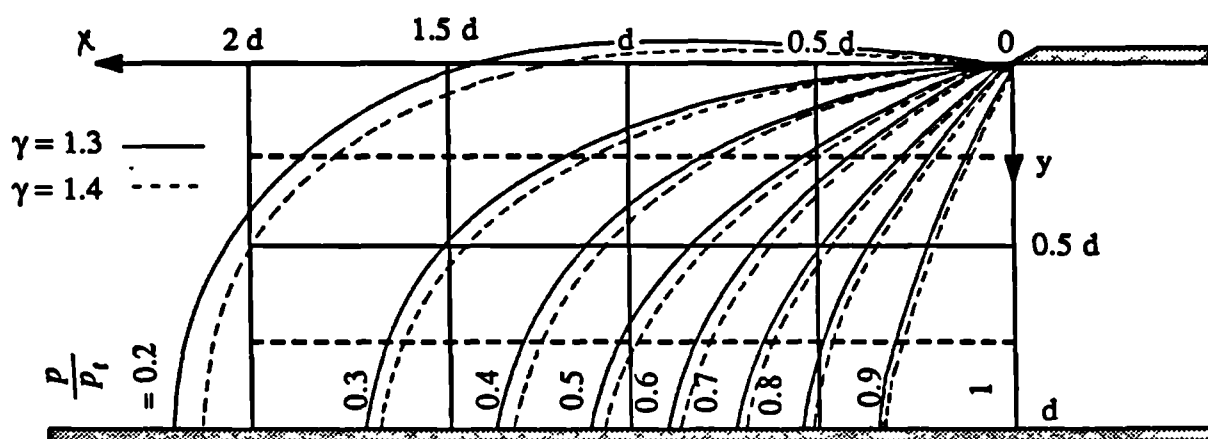


Figure 2.11 Constant pressure contours (isobars) in the discharge region showing gas discharge from duct to the atmosphere (Alder, 1977).

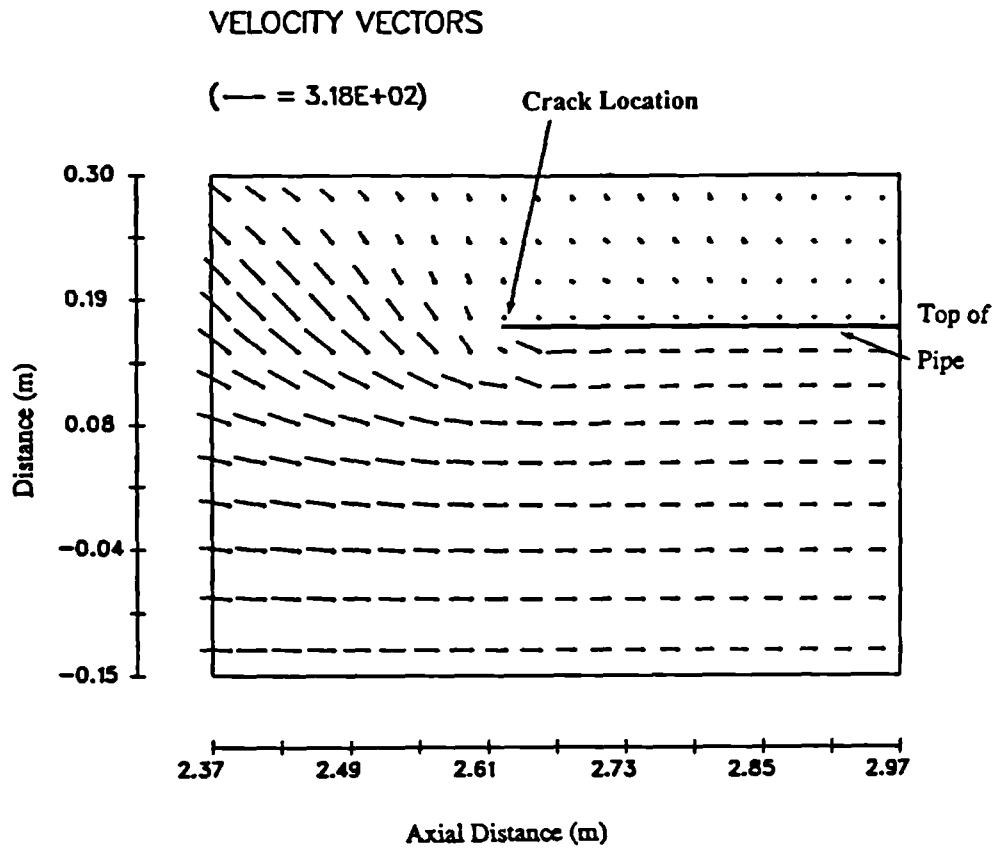


Figure 2.12 Velocity vectors in symmetry plane in front and behind the crack tip calculated by SwRI's coupled FE code (O'Donoghue *et al.* (1989).

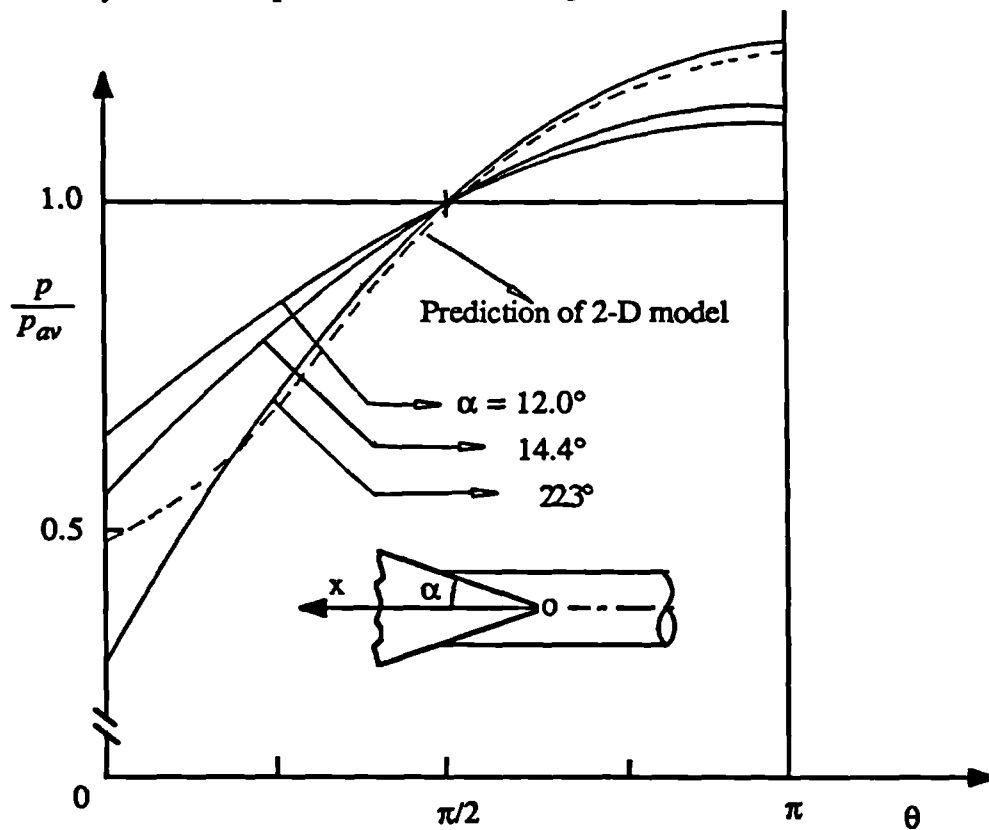


Figure 2.13 Comparison between pressure variation around circumference measure by Alder (1974) and the prediction of 2-D model.

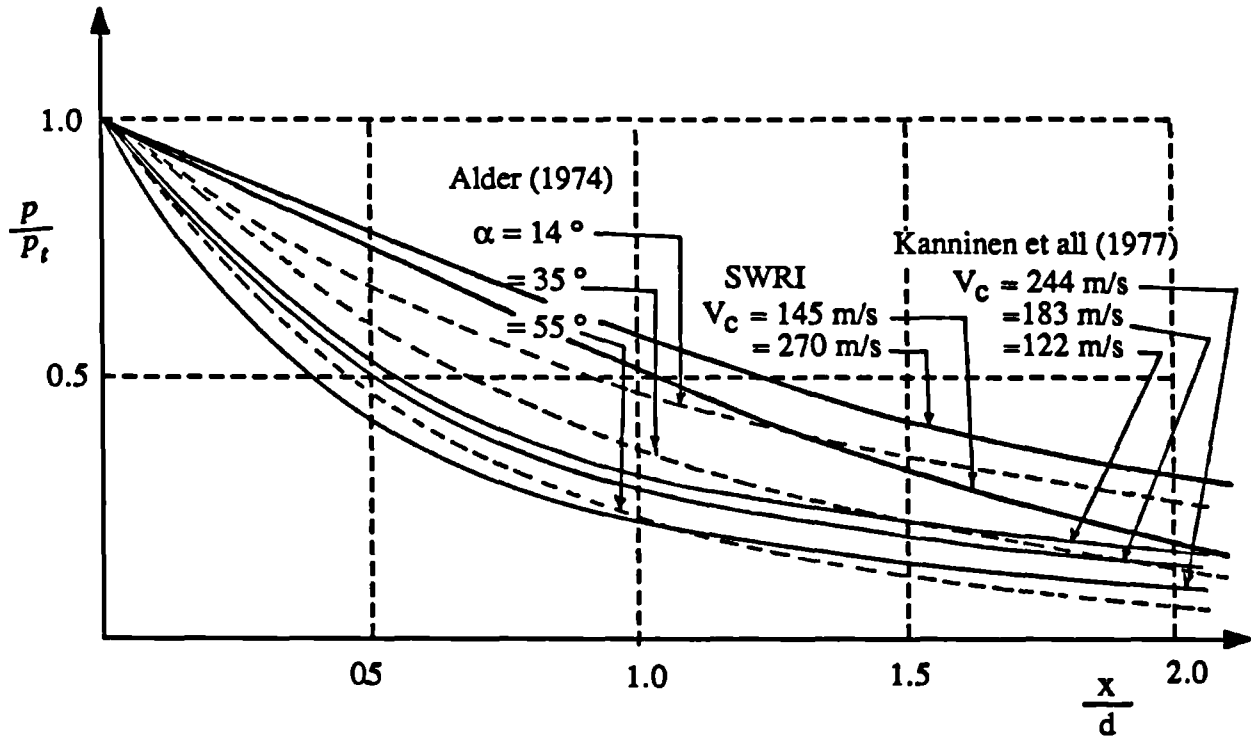


Figure 2.14 Comparison of the models for pressure profile behind the crack tip.

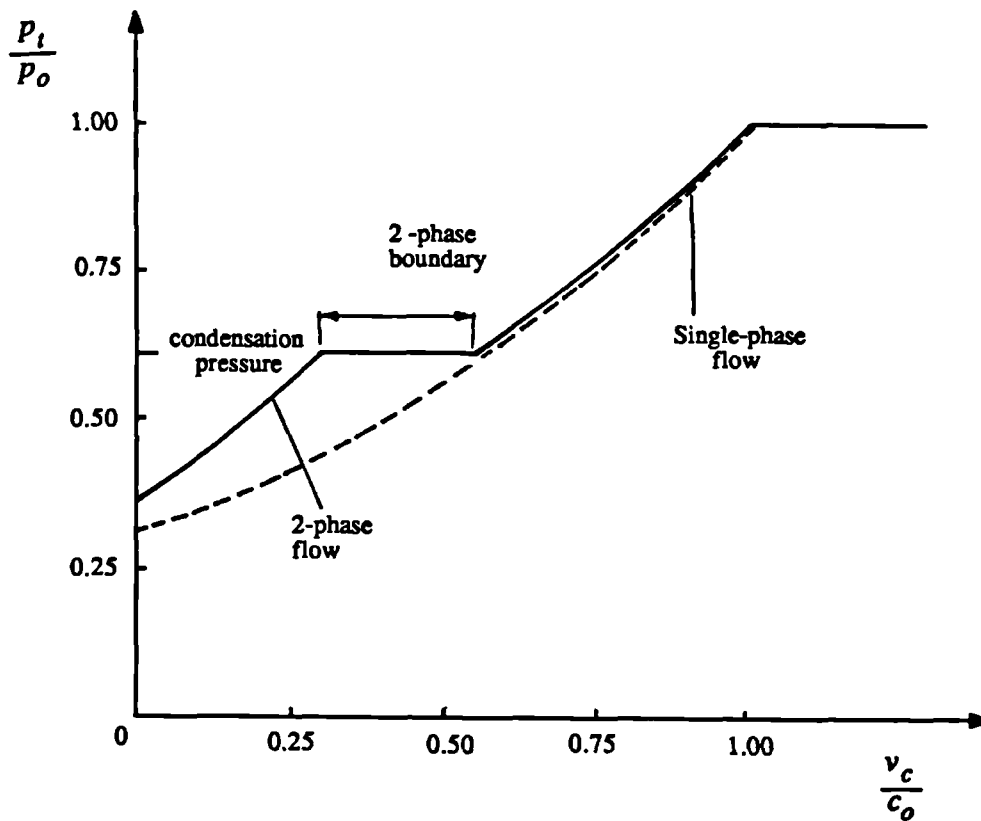


Figure 2.15 Variation of crack tip pressure with crack velocity for gases having condensation during the decompression.

CHAPTER 3

RESIDUAL STRESSES IN POLYETHYLENE PIPES

3.1 Introduction

Residual stresses in tubular structures and pipe have been an area of concern for several years and special methods have been developed to analyse residual stresses in thin as well as thick walled metallic pipes. Some effort has been made to measure the residual stresses in plastic pipes, using the same methodology, taking into account the fact that plastics are time dependent materials.

Residual stresses are introduced into plastic pipes during extrusion of molten polymer, and by the difference between outer and inner cooling rates. The outer surface is cooled by a water bath, the bore is cooled by the trapped air. Heat removal from the pipe wall is almost entirely from the exterior surface of the pipe and so a solid external layer is formed, rapid cooling of the internal surface of the pipe being prevented by the low thermal conductivity of the polymer. While cooling continues the material is prevented from contracting towards the inside of the pipe by the solid outer layer. A *residual stress* system develops in which the compressive stress in the outer layers is balanced by tensile stress in the inner layers so as to maintain equilibrium. Two components of these residual stresses are the hoop stress which is supposed to affect pipe stress fracture performance, and the axial stress which can be made apparent by a reduction in diameter at cut pipe ends.

If the pipe could be cooled at the same rate from both outer and inner surfaces (a less common technique), a symmetrical stress state would be achieved, with compressive stress at both the inner and outer surfaces and with tensile stress in the pipe wall at mid thickness.

The presence of asymmetrical residual stress is easily demonstrated by overlap or opening of the pipe when it is cut axially. For different thicknesses this overlap or opening is shown in Fig. 3.1. Depending on the process of pipe production and cooling technique selected two different types of stress profile are seen in PE pipe, Fig. 3.2. This stress distribution may prevent or slow the growth of cracks from surface defects or notches in PE pipe. The maximum value of residual stress is affected by the melt temperature and the cooling rate. These two parameters also affect the microstructure of the pipe, such as crystallinity and molecular orientation.

3.2. Experimental Techniques

3.2.1. Ring Splitting Method

One way to measure the residual stress in pipe wall is the *ring splitting method*. The maximum value of pipe wall residual stress acting along the longitudinal and circumferential directions can be estimated using the ring splitting method (Bhatnagar & Broutman, 1985). To determine the circumferential stress, rings 25 mm in length are sliced from the pipe and the rings are then slit. If the pipe has tensile stresses on the inside wall the slit ring will close, thus reducing its diameter. The diameter of the pipe is re-measured after an appropriate time period. The residual circumferential maximum stress can be calculated from the expression below:

$$\sigma = \pm \frac{E(t) t}{1-\nu^2} \cdot \frac{D_2(t) - D_1}{D_2(t) \cdot D_1} \quad (3.1)$$

where;

$E(t)$: the creep modulus at time t

t : the wall thickness of the pipe

ν : Poission's ratio

D_1 = mean diameter of the pipe before slitting

D_2 = mean diameter of the pipe after slitting at time t

This method assumes a linear stress distribution across the pipe wall. The method is useful for a rapid comparison of residual stress levels in pipe produced by similar production methods but for different cooling regimes it may lead to misleading results. Thus, the slitting method gives only an approximation to the actual stress distribution. It tends to underestimate the compressive stress on the outer surface, and to overestimate the tensile stress on the inner surface.

It is important to note that the ring splitting method is only valid when the residual stress state is not symmetrical, as in Fig. 3.2a, so that bending occurs. Symmetric stress states as shown in Fig. 3.2b do not create a bending moment, and the cut ring will not open or close. For this case the layer removal method or the drilling method has to be used for residual stress evaluation. It should not be assumed that when the slit ring does not open or close there are no residual stresses.

Using the slitting method, Beech *et al.* (1988) have evaluated the mean levels of stress shortly after production, in 315 SDR 11 and 500 SDR 17 MDPE water pipe grade of the order of 4.5 MPa.

3.2.2. Layer Removal Methods

The value of residual stress in plastic pipe acting on both the longitudinal and circumferential direction can also be estimated using the layer removal technique, which is based on the method of Treuting & Read (1951). The geometry and related dimensions considered in residual stress measurements is shown in Fig. 3.3. This method, first applied to metals, and later by Williams *et al.* (1981) to plastic pipe, involves the machining of successive surface layers, slitting of the rings, and measuring the gap displacement as a function of wall thickness. Time dependent elastic stress analysis is used to convert the measured deformations to the residual stress profile using the following equation:

$$\sigma_{\theta}(h) = \frac{E}{12 \pi a} \left[4 h \delta + h^2 \frac{d\delta}{dh} + 2 \int \delta dh \right] + \sigma_0 \quad (3.2)$$

where σ_0 is a constant which can be found from the zero hoop load condition:

$$\int_0^{h_0} \sigma_{\theta} dh = 0 \quad (3.3)$$

in the above equation;

σ_{θ} : residual hoop stress

δ : gap displacement at time t

h : current wall thickness

E : creep modulus at time t

a : original inside radius

h_0 : original wall thickness

A typical set of data is plotted as δ versus h/h_0 for each machined ring and fitted by a second order polynomial:

$$\delta = b_0 + b_1 (h/h_0) + b_2 (h/h_0)^2, \quad (3.4)$$

which can be substituted into Eqs. 3.2 and 3.3 to give :

$$\sigma_{\theta} = \frac{E h_0}{12 \pi a^2} \left[6 b_0 \left(\frac{h}{h_0} \right) + 6 b_1 \left(\frac{h}{h_0} \right)^2 + \frac{20}{3} b_2 \left(\frac{h}{h_0} \right)^3 - (3 b_0 + 2 b_1 + \frac{5}{3} b_2) \right] \quad \dots(3.5)$$

for outside machining and

$$\sigma_{\theta} = - \frac{E h_0}{12 \pi (a + h_0)^2} \left[6 b_0 \left(\frac{h}{h_0} \right) + 6 b_1 \left(\frac{h}{h_0} \right)^2 + \frac{20}{3} b_2 \left(\frac{h}{h_0} \right)^3 - (3 b_0 + 2 b_1 + \frac{5}{3} b_2) \right] \quad \dots(3.6)$$

for inside machining.

There is a restriction on the layer removing method in that when the remaining wall thickness is less than 20% of its original value, the deflection data are suspicious and the measured deflection could be anomalous because of the disturbance of residual stresses at thin thicknesses during the down process. Thus, determination of the entire stress state through the pipe wall by layer removing method requires a combination of the both outside and inside machining methods.

In contrast to the slitting methods the layer removing method provides the complete picture with an indication of the levels of the surface stresses. The method gives reasonable results when the following conditions are satisfied (Treuting & Read, 1951);

- The specimen is linear in pure bending for the range of curvature.
- The stress does not vary in the plane of specimen but only through the pipe thickness.
- The removal of successive layers does not disturb the stress state in the remaining material.

Hodgkinson & Williams (1983) proposed an alternative method which does not require creep modulus data. In this method, the slit rings having different thicknesses are put in a testing machine and loaded as shown in Fig. 3.4a. This loading results in a load-deflection diagram as shown in Fig. 3.4b. The abrupt change in slope at load P_1 is when the deflection at the cut point becomes non-zero, and the applied moment at this load is then related to M by

$$M = P_1 a \left(\frac{1}{2} + \frac{1}{2\pi} \right) \approx 0.66 P_1 a \quad (3.7)$$

If M_1 is the value obtained for the ring of initial full thickness then;

$$\sigma_1 = 6 M_1 / h_0^2 \quad (3.8)$$

Eq. 3.4 can be rewritten in terms of $\sigma' = \sigma/\sigma_1$, $\delta' = \delta/\delta_1$ and $x=h/h_0$ so that

$$\sigma' = \frac{1}{3} (4x\delta + x^2 \frac{d\delta'}{dx} + 2 \int_0^x \delta' dx) + \sigma_0' \quad (3.9)$$

Defining a parameter $X = M / h^3$ and replacing δ' in the above equation by $X' = X(h_0^3/M_1)$ could allow us to determine σ' .

For this case, 'X' is found as a function of x and hence σ' , and then σ_1 can be found directly from M_1 without resource to a modulus value.

The main disadvantage of this method is that the full residual stress profile cannot be obtained, thus the remaining part of the curve has to be predicted from the equilibrium condition.

It has been pointed out by Wong & Broutman (1983) that anomalous ring deflection data has been observed for turned rings when the remaining wall thickness is less than 30% of its original value. Thus determination of the entire stress distribution profile through the wall by the turning method has to rely on extrapolation of the data, which can cause some errors. For that reason, a combination of boring (removing layers from inner surface) and turning (removing layers from outer surface) is supposed to yield the stress distribution more accurately.

3.2.3. Strain-Gauge Method

Chaoui *et al.* (1988) used the *strain-gauge method* to obtain the stress distribution within the pipe wall. The strain gauge technique, like the others, involves removing material from the surface(s). Perhaps the main advantage of the method is that the strain in a particular direction can be readily measurable. Like the other stress measurement techniques, the strain gauge technique has some unresolved limitations. These include the effect of localised heating on adhesion and sensitivity of the gauges, material nonlinearity, structural changes caused by removal of layers, and possible nonuniformity of elastic properties across the pipe wall.

3.2.4. Hole Drilling Method

If a hole is introduced into a residually stressed material the stresses surrounding the hole relax, the stress normal to any free surface being zero, the local surface strains change correspondingly. Measurement of these strains provides the necessary data to define the residual stress distribution. Although the technique has advantages in speed and cost, it is shown by Broutman *et al.* (1985) that the calibration of the test parameters requires special attention. Further details are given in Broutman *et al.* (1986).

3.4. Effect of Residual Stress on Performance of PE pipes

The effect of residual stress on mechanical behaviour of metals has been well studied in the past. Residual stresses apparently influence solvent stress cracking, brittle fracture strength and impact strength of polymers but it is a fact that these effects do not appear to be well researched or documented.

Greig (1985-b) pointed out that the critical stress for arrest of a rapidly propagating crack in a 250 mm SDR 17.6 PE pipe at 0 °C produced with simultaneous outside and inside surface cooling is 30% higher than for a standard outer surface cooled pipe. From similar tests on 315 mm SDR 17.6 pipes, he observed that both externally and internally cooled pipe gives 10% higher critical pressure compared with only externally cooled pipe.

Crack propagation behaviour in single edge notched specimens prepared from MDPE pipe was examined under creep conditions by Chaoui *et al.* (1987). They observed that a crack, grown from an exterior notch, initiated faster than one grown from an interior notch. Subsequently the outbound crack propagated monotonically to ultimate failure. Using the ring splitting method, Broutman *et al.* (1988) studied the effect of residual stress in PE pipes, pointing out that the measured residual stress varies significantly for different types of pipes. This is attributed to variations in the production processes. Ingen Housz (1982) developed a modified cooling system by which the pipe was cooled simultaneously at the outside and inside of the pipe. This system was supposed to have the following advantages:

- The residual stress pattern over the pipe wall is changed ; the tensile stress at the inner surface is changed into a compressive stress. Thus the maximum tensile stress no longer occurs at the inner surface.
- For a given extrusion velocity, compared to conventional system, the required cooling length decreases.

Bhatnagar & Broutman (1985) studied the effect of annealing of the pipe on the residual stress. They observed that annealing reduced and in some cases eliminated the residual stress, and increased the degree of crystallinity. It was also observed that annealing of the pipes caused a reduction in diameter. The reason for this was given as the frozen stress or orientation as a result of expansion of the pipe during the sizing process shortly after exit from the extruder die. Thus, annealing allows the pipe to recover from its oriented state. Annealing also increased the yield strength, but it was not well understood whether effects on mechanical properties were due to an increase in crystallinity or the relaxation of residual stresses. The sustained pressure tests performed by Broutman *et al.* (1985) on PE pipe also showed that both slow crack initiation and propagation resistance were increased due to annealing.

Because of the viscoelastic nature of thermoplastic parts the stress in the product will relax with time. Beech *et al.* (1988) pointed out that residual stress decays rapidly during the first month after production, but Broutman *et al.* (1988) observed that there is very little or no decrease over the service lifetimes. They also concluded that whatever the effects of residual stresses on PE performance are, they are just as likely to have an effect on 20-year old pipe as on newly produced pipe.

3.5. Comparison of the Residual Stress Results Obtained from Different Techniques

Using different techniques, the distribution of the residual stress in PE pipe has been studied by Williams *et al.* (1981), Chaoui *et al.* (1988), Broutman *et al.* (1985). Although the

reported residual stress results differ from each other, a common feature is that the residual stresses are compressive on the outer pipe surface and tensile at the inner surface. In Fig. 3.5, the residual stress distribution in MDPE, 2306-IIC, obtained by Chaoui *et al.* (1988), using strain-gauge technique, is given. From the figure it can be seen that the residual stress obtained from strain gauge technique is much lower than that obtained from the layer removing method.

It has been pointed out by Beech *et al.* (1988) that the measured values of residual stress are dependent on the width of the ring cut from the pipe; and there is a relatively sharp decrease in the value of measured stress for specimens of less than one diameter in width. This is attributed to the presence of both hoop stress and axial stress forming a biaxial stress field in longer specimen. For that reason when the results for different pipes and techniques are compared the specimen length is to be known and should be taken into account.

To generalize the crack front shape in RCP in PE pipes is not possible, since the crack front profile varies with the position of the crack tip on the oscillating crack path. But in general, as can be seen from Fig. 3.6 the crack extends further along the inner surface than the outer surface. This may be due to the stress gradient in that the radial displacements of the flaps behind the crack tip, created by the pressure acting on them, creates higher both axial and circumferential stresses at the inner surfaces around the crack tip. In addition to this, the value of the residual stress is tensile at the inner and compressive at outer surfaces. When these two stresses are combined together, the total tensile stress on the inner surface is higher than that on outer surface. Fig. 3.7 shows this stress state in the PE pipe schematically. The resultant hoop stress profile can take many forms, it is always tensile at the inner section but for the outer section, depending upon the hoop stress and the residual stress profile, it may be positive or negative, but is always well below that on the inside.

The fracture surface analysis of the present work indicated that at lower pipe pressures, the effect of this stress gradient on the appearance of fracture surfaces has many features. At the inside of the pipe wall, the surface is almost smooth, but at the outside it is very complicated,

depending upon the place of the fractured surface along the sinusoidal wave form. As the level of pressure increases and the crack speed goes up, its effect greatly diminishes.

3.6. Experimental Residual Stress measurements

3.6.1. Experimental Procedure

Circumferential residual stress was evaluated in 180 SDR 17.6 PE pipe samples using the method of Williams *et al.* (1981). Two grades of materials were used; BP Rigidex 002-40, which was supplied by Stewart & Lloyds Plastics and Du Pont Aldyl 'A' supplied by Du Pont (UK) Ltd. 25 mm long rings were cut from pipes. During the turning process, for outside machining a mandrel having a slightly smaller outside diameter was inserted into each ring to provide the rigidity needed to prevent any disturbance in the residual stress profile. For inside layer removing a special ring having slightly bigger inside diameter was prepared and the specimens were inserted into the ring. The cutting increment was 0.5 mm for the coarse machining; when approaching the prescribed thickness the cutting increment was reduced to 0.25 mm.

3.6.2. Residual Stress Measurement Results

Two methods described above were used to measure the residual stresses in PE pipes, using variations of the layer removal method. These are measurement of the deflection of the cut rings after a certain time, generally 600 hours (deflection analyses) and of the ring opening bending moment (discontinuity analysis). Beside these two methods, the maximum value of the residual stress on the outer surface of the pipe was also calculated using the split ring method. The second technique requires considerably less time to carry out than the deflection analysis. In addition to this, the discontinuity analysis does not require the creep modulus of the material. The creep modulus for the BP 002.40 pipe at 600 hours was taken as $0.146 \times 10^9 \text{ Nm}^{-2}$.

The measured residual stress results showed that for deflection analysis both inside and outside machining gave almost the same residual stress profile. For the pipe tested, these are:

The maximum value at outside of the pipe, which is compressive, is around 1.8 MPa and at the inside, which is ~~tensile~~ , is 0.9 MPa for BP 002-40 180 SDR 17.6 gas pipe. The variation of the final deflections for different thickness for outside machined Rigidex pipe is plotted in Fig. 3.8. The stress profile calculated from the outside machined deflected rings is given in Fig. 3.9 and for inside machined pipe in Fig. 3.10. For Aldyl 'A' and Rigidex pipes the discontinuity loads for different thickness are given in Fig. 3.11. For Aldyl 'A' and pipe the stress profile obtained from this curve by using 'discontinuous analysis' are given in Fig. 3.12 for inside machined pipe and in Fig. 3.13 for outside machined pipe. The profile obtained with this technique is similar to that of deflection analysis.

The approximate stress profile for Rigidex pipe was also determined using slit ring technique. The profiles evaluated from 'discontinuity analysis' and 'split ring analysis' are given in Fig. 3.14. Using this method the maximum stress at outer section was found to be - 1.5 MPa. The residual stress measurement results show that the slitting method tends to underestimate the compressive stress on the outer section of the pipe but overestimates the tensile stress on the inner surface.

The residual stress profile for Aldyl 'A' pipe was also determined using the discontinuity analysis. Other techniques could not be used to define the stress profile, since they require the material's creep modulus (which was not available when the analysis was carried out). For comparison the measured deflection results at 600 hours for BP 002-40 and Du Pont Aldyl 'A' 180 SDR 17.6 pipe are plotted in Fig. 3.15 for inside machined pipes. For the Aldyl 'A' pipe the stress profile calculated from discontinuity analysis is shown in Fig. 3.12 and Fig. 3.13.

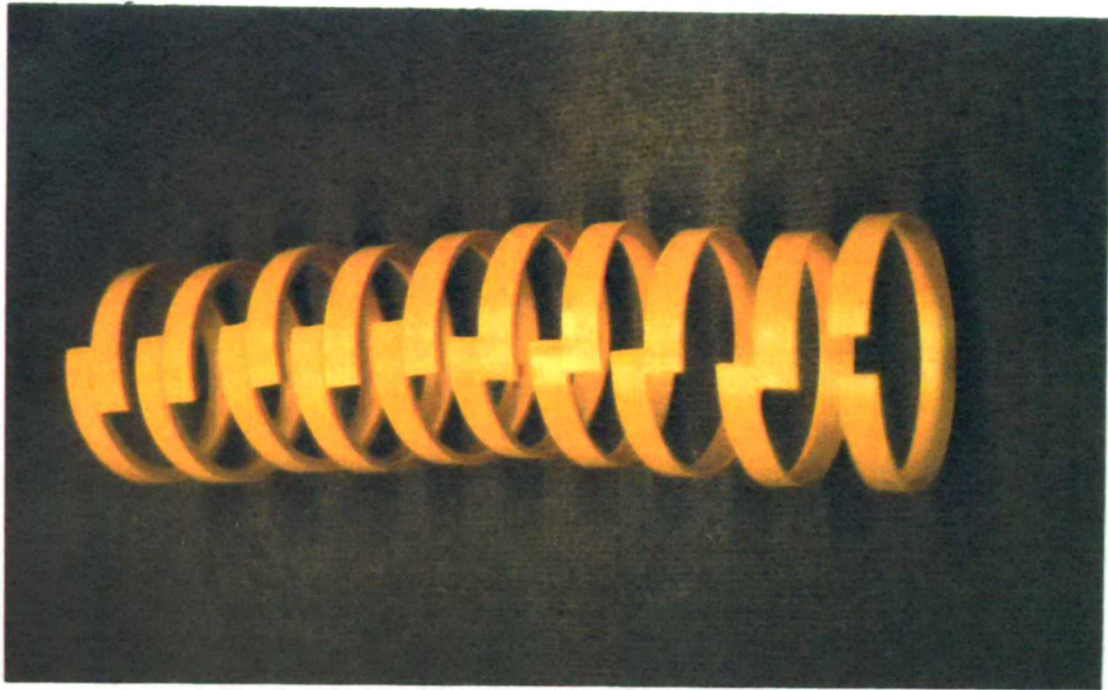
3.7. Conclusions

Polyethylene pipe being used for gas distribution systems has a residual stress created during production process. Whilst the influence of this stress on pipe life is not fully understood, the dependence upon extrusion cooling process of the magnitude and profile of this stress is

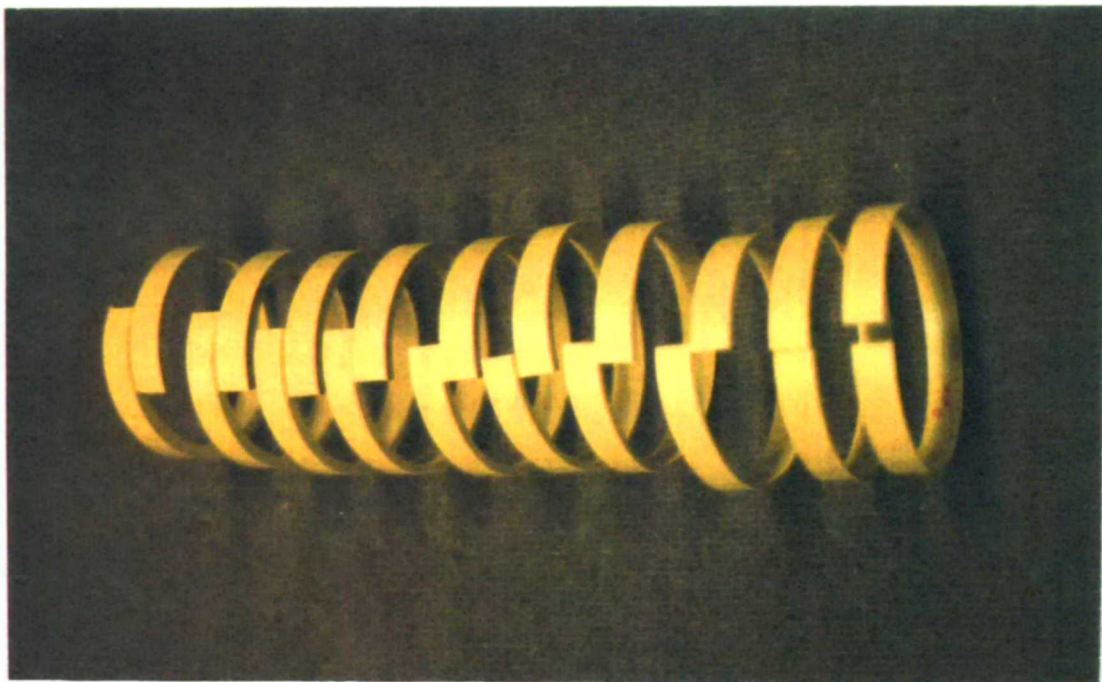
well defined by different techniques. The residual stresses are generally compressive at the outer surface and tensile at the inner surface.

The split ring method provides a single value of stress for the outer section of the pipe, which might be an useful method for a rapid comparison.

The layer removing method provides the complete profile of residual stress through a pipe wall. The tensile stress at the inner surface generally ranged between 0.8 - 1.1 MPa while the compressive stresses at the outer surface ranged between 1.8 - 2.8 MPa.



(a)



(b)

Figure 3.1 The deformation of rings cut from (a) BP 002-40, and (b) Du Pont Aldyl 'A', 180 SDR 17.6 PE pipes, showing the deflection of rings having thicknesses decreasing from 10mm to 1mm in 1mm-steps (left to right in the photographs), due to residual stresses in the pipe walls.

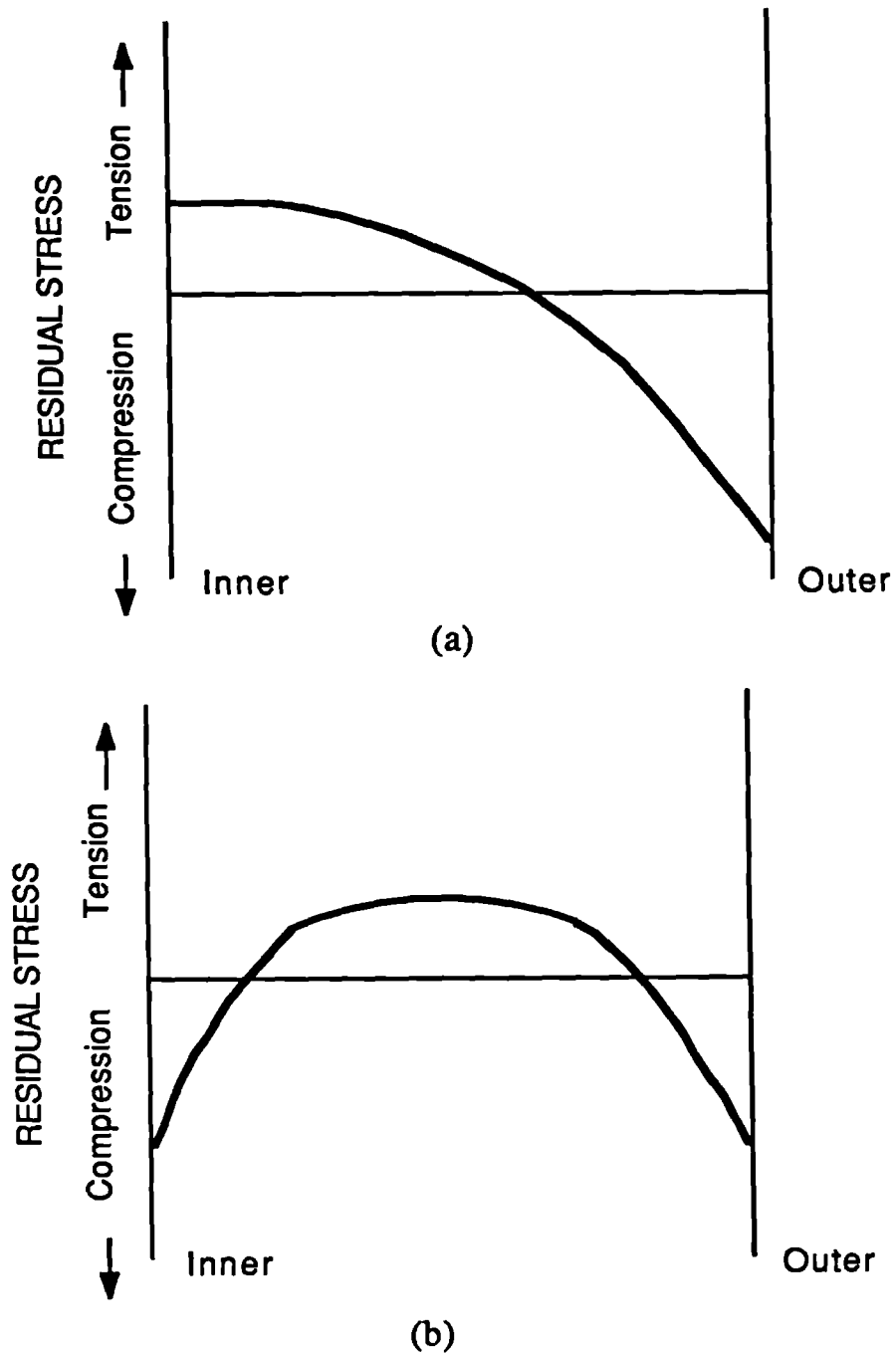


Figure 3.2 Possible residual stress distribution profiles through pipe wall.

(a) Stress profile in externally cooled pipe,

(b) Stress profile in both externally and internally cooled pipe.

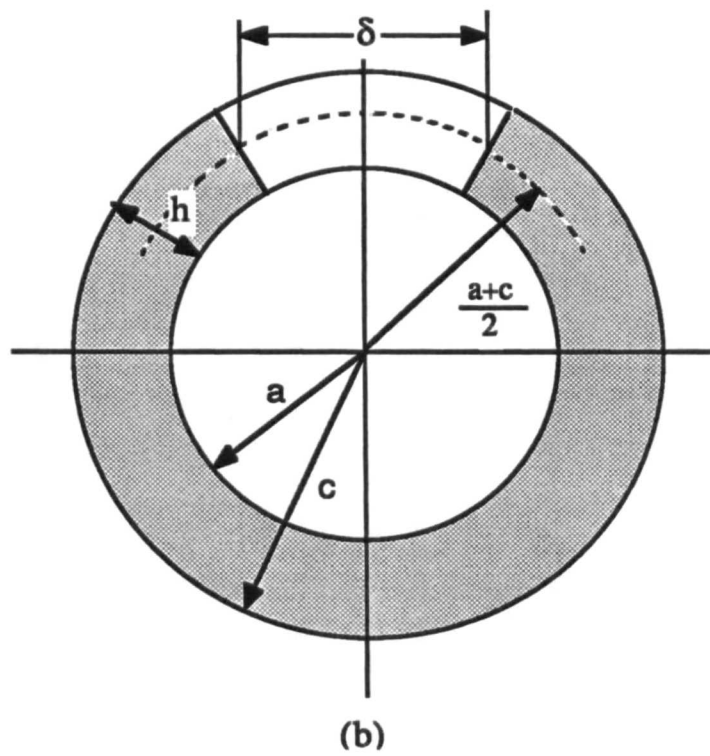
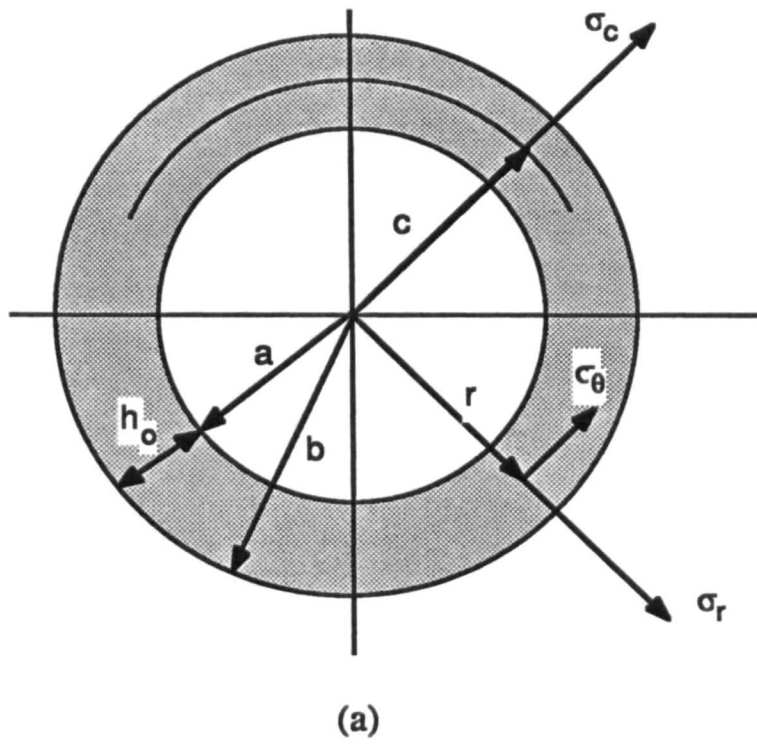


Figure.3.3 The geometry considered in residual stress measurements

a) Ring Section geometry

b) Ring deflection geometry.

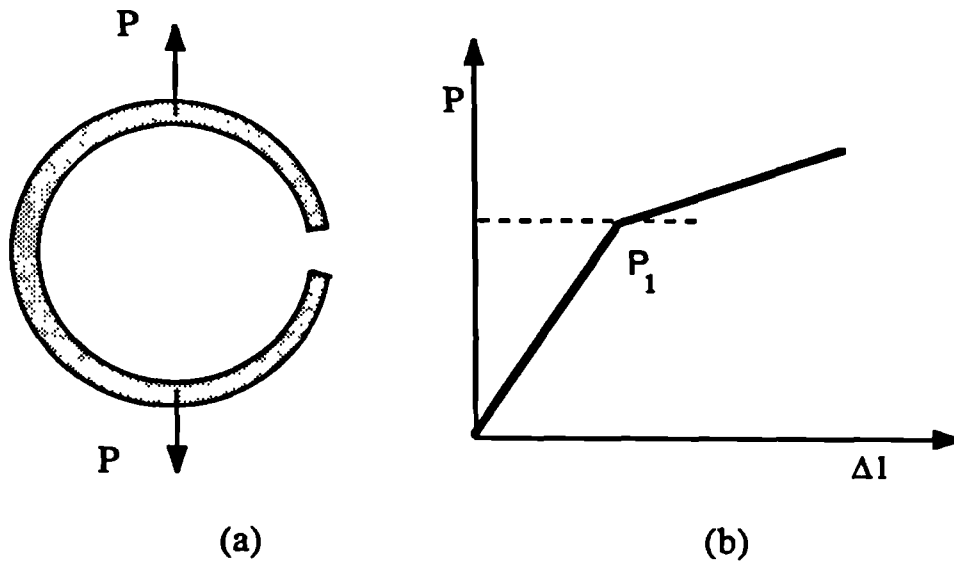


Figure 3.4 Determination of bending moment by load testing.

(a) Loading condition

(b) Load-displacement curve

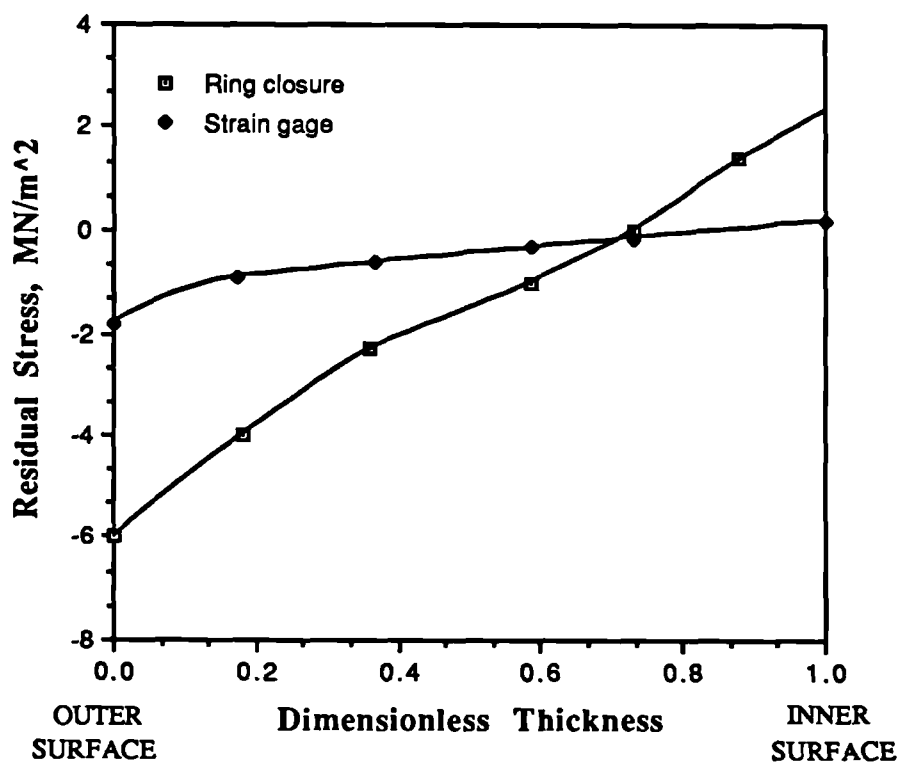


Figure 3.5 Residual stress profile from strain-gauge technique (Chaoui *et al.*, 1988).

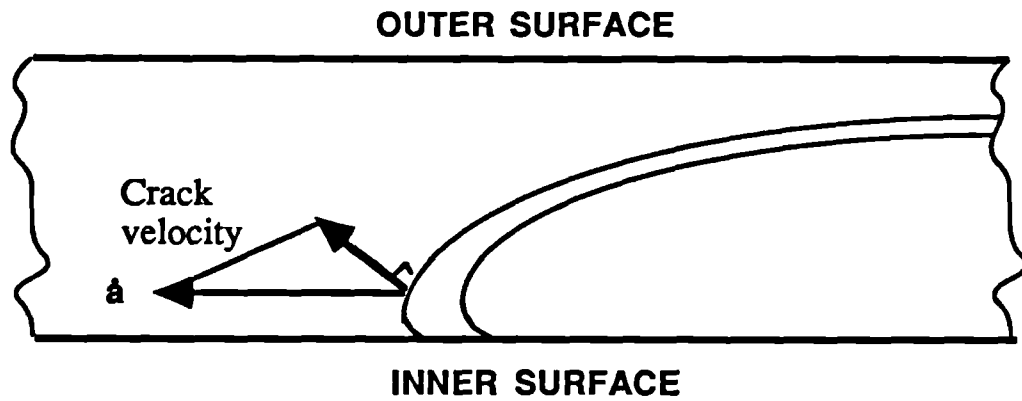


Figure 3.6 The profile of a fast propagating crack in gas pressurized PE pipe.

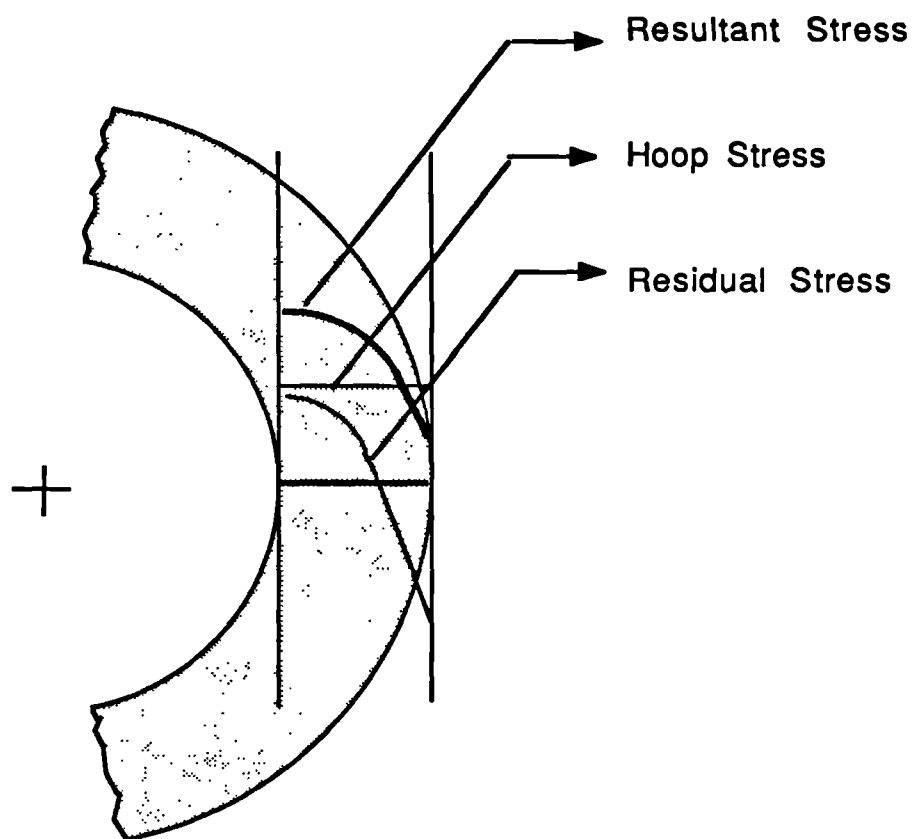


Figure.3.7 The schematic stress state in a pressurized PE pipe only cooled externally.

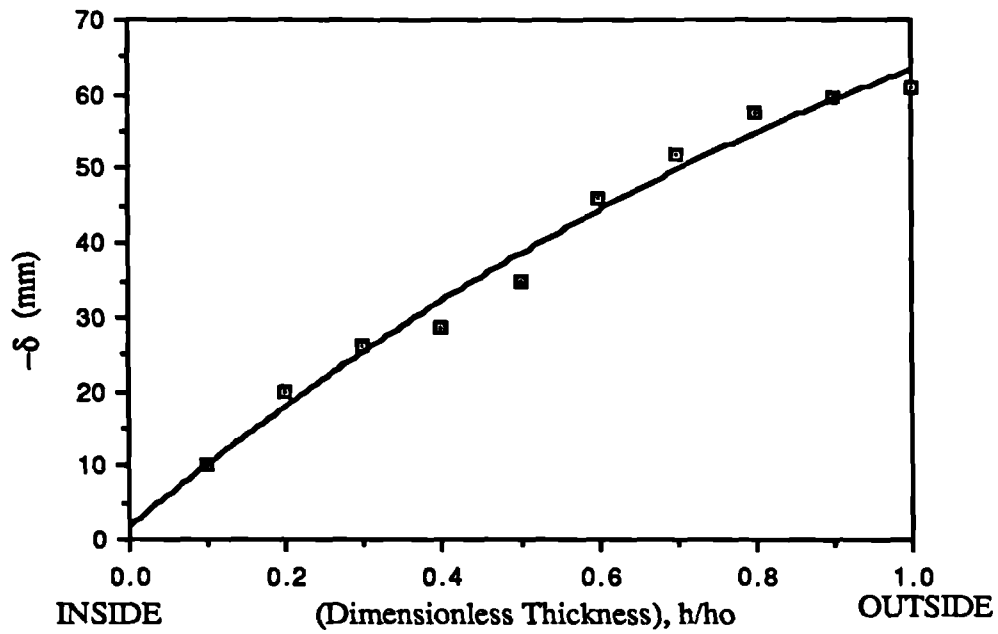


Figure 3.8 Deflection of split rings for Rigidex 002/40 180 SDR 17.6 pipe (machined from outside, deflection analysis)

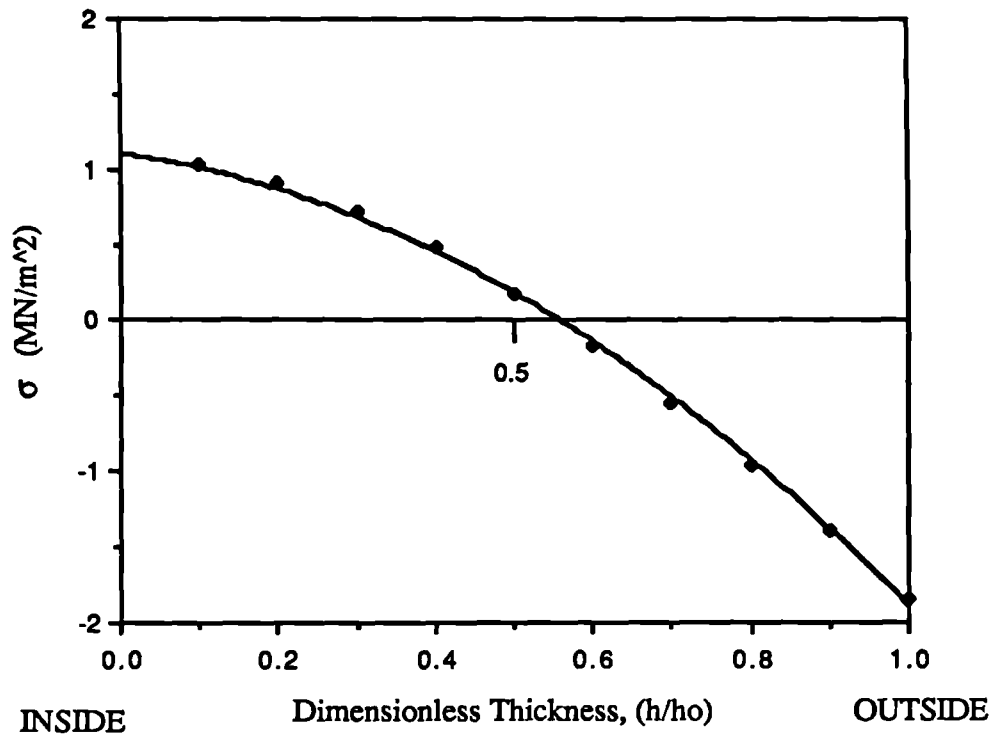


Figure 3.9 Residual stress profile for Rigidex 002/40 180 SDR 17.6 pipe (Machined from outside, deflection analysis).

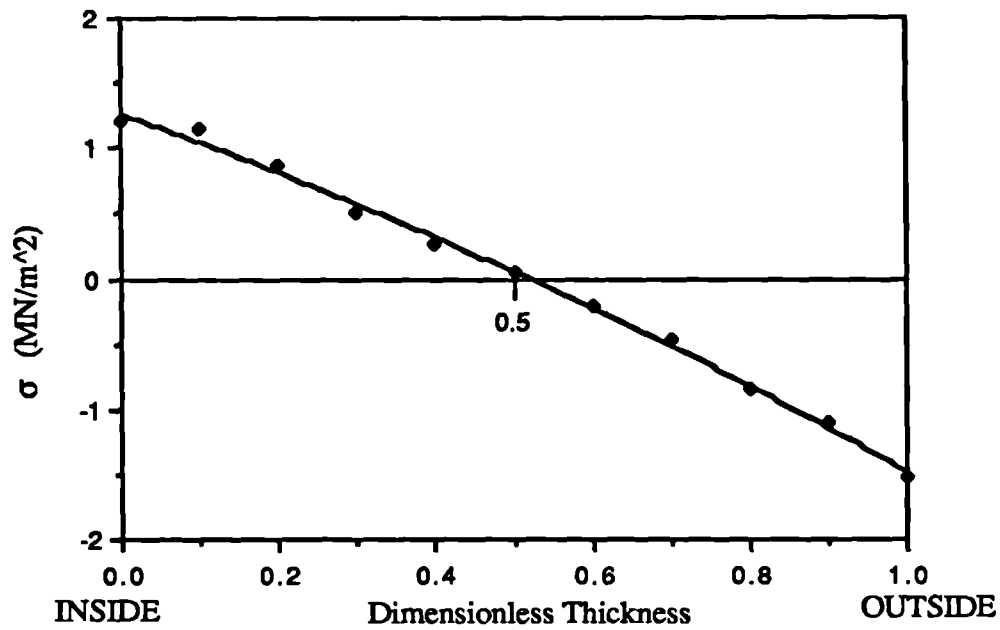


Figure 3.10 Residual stress profile from deflection of split rings for Rigidex 002-40 180 SDR 17.6 pipe (machined from inside, deflection analysis)

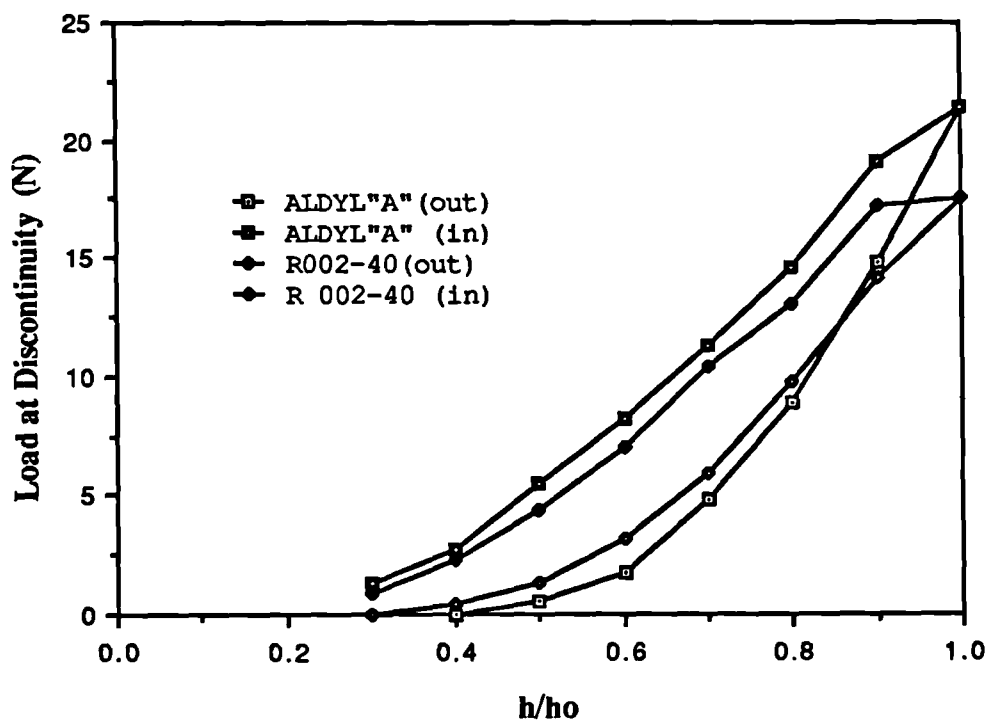


Figure 3.11 Discontinuity load for both inside and outside machined 180 SDR 17.6 Rigidex 002-40 and Aldyl 'A' 180 SDR 17.6 pipes.

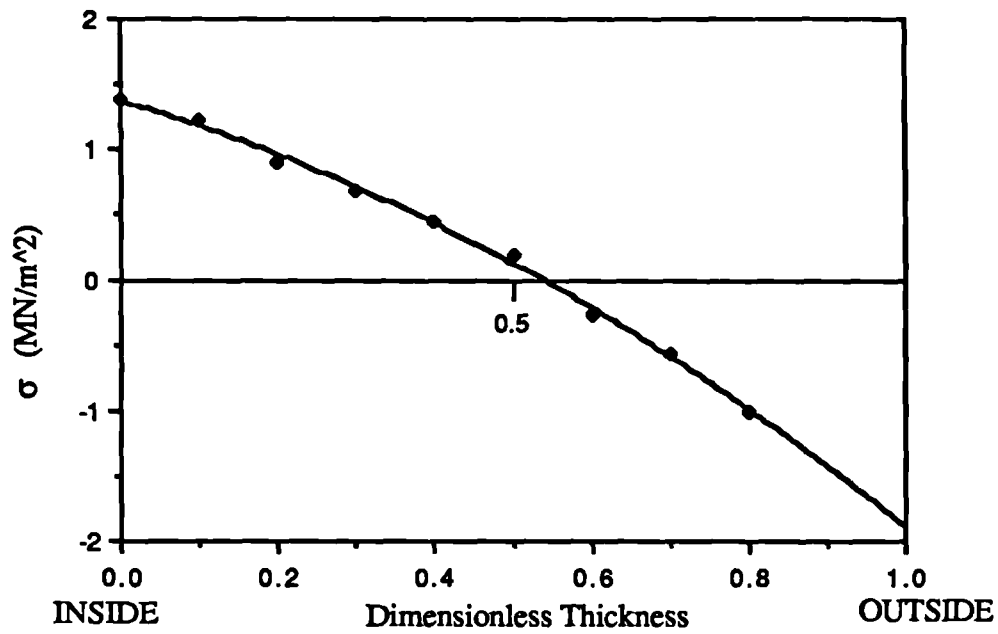


Figure 3.12 Residual stress profile for Aldyl 'A' 180 SDR 17.6 pipe (machined from inside, discontinuity analysis).

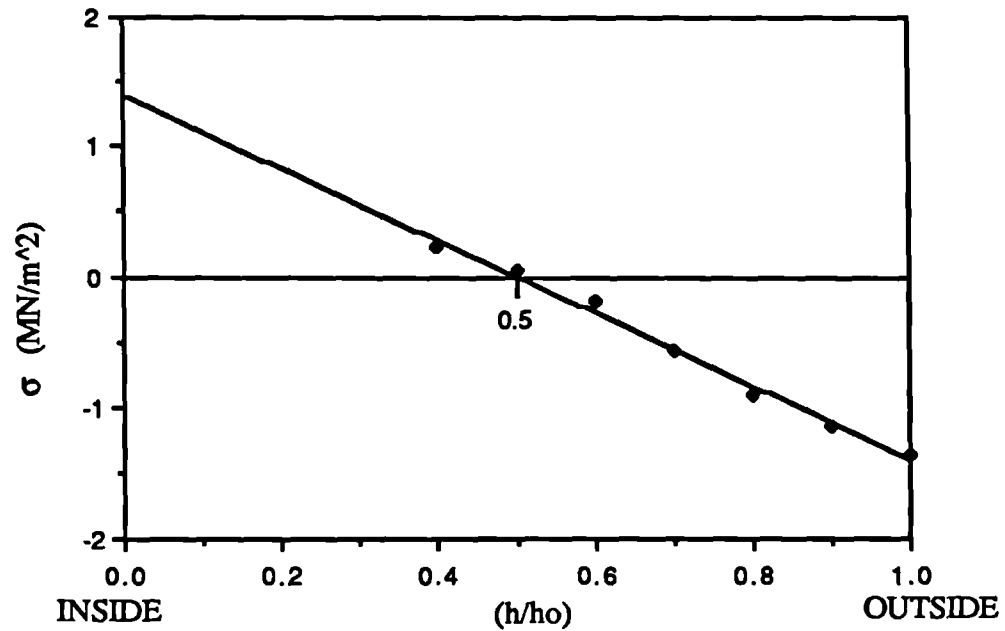


Figure 3.13 Residual stress profile for Aldyl 'A' 180 SDR 17.6 pipe (machined from outside, discontinuity analysis).

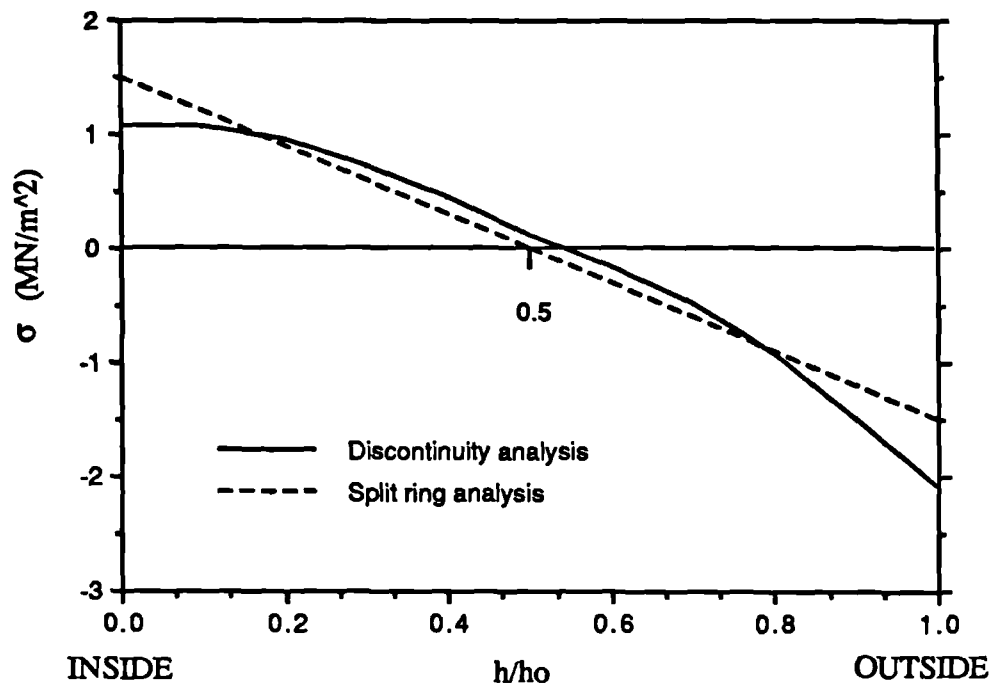


Figure 3.14 Residual stress profile for BP Rigidex 002/40 grade 180 SDR 17.6 pipe (machined from inside, discontinuity and split ring analyses).

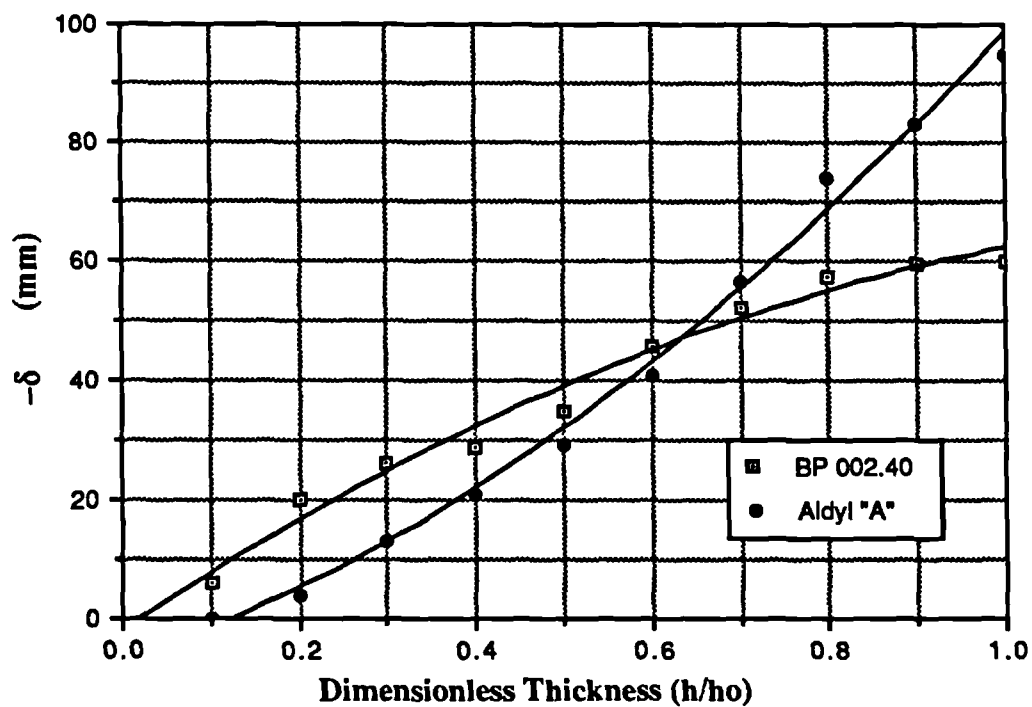


Figure 3.15 Comparison of deflection of rings for Rigidex 002.40 and Aldyl 'A' 180 SDR 17.6 pipe after 600 hours (machined from outside).

CHAPTER 4

DYNAMIC MECHANICAL PROPERTIES OF PIPE GRADE PE

4.1. Introduction

This chapter presents and reviews the available short term mechanical properties of MDPE and HDPE pipe grades. The main properties that were considered here are dynamic fracture resistance and elastic modulus obtained by different authors using different test techniques. Moreover, the densities of PEs that were used in the present study are also given. Finally, due to the lack of available fracture toughness of the pipe grade of PEs which were studied a series of Charpy test result for a wide of temperature is presented and the relevance of the results to observed RCP performance is discussed.

The study of dynamic behaviour of polymers is of interest for practical as well as academic reasons. As we have seen, any pipe failure analysis will ultimately reduce to a set of equations involving the pressure and physical properties of the contained fluid, and the dimensions and mechanical properties of the pipe (and perhaps, weakly, the properties of the backfill). We now consider the methods available for evaluating pipe material modulus and fracture toughness data relevant to the RCP process, as required by LEFM analysis. It is well known that the mechanical properties of polymers are both strain rate and temperature dependent. Additionally, relatively small changes in molecular structure alter the performance, significantly.

4.2. General Background

Polyethylenes (PE) are characterised by toughness, near zero moisture absorption, excellent chemical resistance and electrical insulating properties, low coefficient of friction and ease of processing.

Polyethylenes are classified by their densities as follows:

Type 1: 910 — 925	kgm ⁻³	low density (LDPE)
Type 2: 926 — 940	"	medium " (MDPE)
Type 3: 941 — 965	"	high " (HDPE)

The density of unfilled and natural (un-pigmented) grades of MDPE used for gas pipe are usually in the 935 to 947 kgm⁻³ range. If the material is pigmented with carbon black (2% minimum level) it will increase the density by about 10 kgm⁻³.

The preliminary differences among the three types are rigidity, heat resistance, chemical resistance, and, more importantly, the ability to sustain loads. In general, hardness, heat resistance, stiffness, and resistance to permeability increases with density. As density decreases, impact strength increases.

Three basic characteristics of PE could be used to characterise the resin's properties. They are:

- i) Crystallinity
- ii) Molecular weight
- iii) Molecular weight distribution

The use of PE in pipe applications has a very good record for over 30 years. The performance of the material is defined by the above parameters. Additionally, the presence of additives also has a significant influence on the materials performance.

It is a fact that the deformation of polymers is a complex subject of great technological and theoretical interest. A number of models have been proposed to describe the various phenomenon that may occur during deformation, such as RCP, brittle and ductile fracture, yielding, crazing, *etc.* (Kinloch & Young, 1983; Williams, 1984). However, most models are only semi-empirical, phenomenological or static representations which are inadequate to provide a unified description of various morphological changes that may take place depending upon the dynamics and on the structure at molecular level.

4.3. Influence of Structural Properties

The mechanical behaviour of a large number of polymers has been widely studied as a function of temperature using different mechanical testing methods. The observed types of behaviour are found to depend upon whether the polymers are amorphous or crystalline in nature. For amorphous polymers, the elastic modulus decreases, in general, as the temperature is increased and drops sharply at a specific temperature, named *glass-transition temperature*. But for semi-crystalline polymers, such as PE, the interpretation of the behaviour could be extremely complex due to their complex two-phase, amorphous and crystalline, structure. The degree of crystallinity has an important effect on the physical properties of a polymer. The more crystalline the polymer, the higher its density. The most common technique to determine the degree of crystallinity in PE is to use density as an indication of crystallinity.

Table 4.1 Relationship between polyethylene characteristics and material properties (Leech, 1989).

<i>Property</i>	<i>Increasing crystallinity</i>	<i>Increasing molecular weight</i>
Modulus	Increases	-
Impact Strength	Decreases	Increases
Low temperature toughness	Decreases	Increases
Hardness	Increases	-
Tensile stress at yield	Increases	-
Tensile stress at break	Increases	Increases
Elongation at break	Decreases	-
Softening temperature	Increases	Increases
Permeability	Decreases	-
Chemical resistance	Increases	-
Melt processibility	-	Decreases
Environmental stress crack resistance	Decreases	Increases

Until recently, the more commonly accepted material for gas distribution system was MDPE. But the improvements on the material's mechanical properties has changed the situation and 'third-generation' HDPE pipe resins have successfully been developed as pipe grade materials. Thus, the use of HDPE as a pipe material has become internationally accepted. Nowadays, the majority of the pipes buried underground in Europe and in USA constitutes this new generation HDPE which has superior properties over conventional MDPE material. Table 4.1 gives a brief description of the influence of different fundamental PE characteristics on material properties. From the table, the complexities of interaction between these two properties could be seen. Furthermore, Leech (1989) also pointed out that the interrelation among these characters could alter these effects.

4.4. Influence of Chemical Composition

Nowadays, there are a number of processes and catalysts used in the production of PE. But much of these technological features and the details of processing technique remain a secret for the companies manufacturing them. However, what they have in common is the fact that the monomer and co-monomer units are combined to give a polymer used in various engineering applications. By compounding various additives into the polymer, PE is prepared for processing. For gas piping system, the additives generally include thermal stabilisers, different pigments or carbon black. But the total is, generally, less than 1% for the pigmented compounds or about 3% for black compounds.

Raske and Young (1989) attempted to correlate the chemical and molecular properties of PE pipe grades to predict the mechanical behaviour. From extensive chemical and molecular analysis and mechanical testing of 12 different PE pipe grades they envisaged that their model could be used to predict the mechanical properties of new pipes and thus be of use to resin and pipe manufacturers in the development of better gas pipes.

4.5. Review of the General Properties

Investigation of the mechanical properties of PE pipe grades has been extensively considered for the last 25 years. The main problem, however, is that the mechanical properties of polymers are both strain rate and temperature dependent.

The commercial names and characteristic features of PE pipe grades tested during the current work are given in Table 4.2.

Table 4.2 The names and Characteristic Features of studied PE pipe grades

Commercial Name	Manufacturer	Colour	Density (kgm^{-3})
Rigidex 002-40	BP Chemicals	Yellow	940 (BP Technigram TIS1/8) 934 (Genussov, 1989) 945 (Truss, <i>et al.</i> 1984)
Rigidex 002-50	BP Chemicals	Blue	950 (BP Technigram TIS1/8) 940 (Genussov, 1989) 950 (Crouch, 1986)
Aldyl 'A'	Du Pont	Yellow	937 (Bragaw, 1980)
Eltex TUB 125	Solvay	Orange	960 (Dioh, 1989)

4.5.1. Elastic Modulus

The modulus of a semi-crystalline polymer such as PE is markedly rate and temperature sensitive. The 'static' (*e.g.* 100 s) value of flexural modulus E may be as low as 0.7 MPa for a pipe grade HDPE at 20°C, and more than three times this value at high rates and at 0°C. The Battelle group (Krishnaswamy *et al.*, 1986 and 1897) used a 'water-hammer' technique to measure the modulus of the material in pipe form. The velocity of an axial pressure wave is directly related to the moduli of the pipe and of the contained fluid, in this case methanol and/or water of known compressibility. This velocity was measured as a function of temperature after impacting one end-plug of the pipe with a pendulum hammer. However, the analysis used to arrive at the pipe modulus value has been severely criticised by

Kanninen (1987), who showed that it would certainly have yielded artificially low results. At Imperial College, Leever & Williams (1987) arrived at rather higher values using a technique measuring the longitudinal stress wave propagation speed through a very small sample. The surprisingly high modulus values yielded by this method, typically 2.4 to 2.5 MPa, illustrate the glassy response to be expected from this rather highly crystalline polymer under such high strain rates, helping us to understand its quasi-brittle fracture behaviour. Gaube & Müller (1980) arrived at very similar values using cyclic bend tests at 1 kHz and 20°C.

Using the ultrasonic immersion method, described in detail by Read & Dean (1978), Crouch (1986) and Dioh (1989) have measured the variation of the dynamic Young's modulus of pipe grades PE with temperature. From these results, plotted in Figs. 4.1 and 4.2, it could be seen that the modulus varies almost linearly with the test temperature (although the results were extrapolated in Fig. 4.1). This observation is an important feature that has to be considered in analysing the critical pressure variation with temperature in pipe-test results.

Using an instrumented impact loaded High Speed Double Torsion (HSDT) test technique, Leever & Williams (1987, 1988) measured dynamic crack resistance of PE grades. Their results showed a large scatter in dynamic crack resistance values up to 15 kJm⁻², giving the lower bounds presented in Table 4.3.

Table 4.3 Dynamic Moduli and crack resistance of PE pipe grades (Leever & Williams, 1987, 1988)

PE Grade	E_D (GPa)	<u>0°C</u>	E_D (GPa)	<u>23°C</u>
		G_D (kJm ⁻²)		G_D (kJm ⁻²)
Rigidex 002-40	1.87	2.5	1.39	4.0
Rigidex 002-50	1.83	2.0	1.39	-

More recently, using the same HSDT technique but strain dependent material modulus Wheel and Leever (1990) reported lower scatter in their results giving 3 - 6 kJm⁻² for Rigidex 002-40 and 4 - 7 kJm⁻² for TUB 125 for the crack velocities in the range of 125 - 240 ms⁻¹

4.5.2. Thermal Response

One of the crucial question in the present experimental investigation is much heating up was taking place during the time required to take the pipe from the conditioning refrigerator and test it. Since the tests were carried out at ambient temperature, it was obvious that the test pipe heated up during the testing process. This problem has been overcome by BG (Greig, 1985-b) by cooling down the test pipe to the required temperature with a temperature controllable trench. It seems that this system has worked adequately well, but it, obviously, brought some extra cost to the experiments. In all the other small-scale test techniques, the pipe specimens were cooled by using a freezer. Although this cooling process is very practical and less time consuming, it is questionable, by how much, the pipe warms up during the testing.

In order to gain a preliminary idea about the temperature rise during the test process, two 6 mm PE plates were melted together with a thermocouple in between, and another on the surface. This sandwiched specimen was put into the chest-freezer and kept there for one day. This cooled specimen was exposed to room temperature ($\approx 18^\circ\text{C}$). The variation of the temperature on the surface and centre with time is shown in Fig. 4.3.

Fig. 4.3 shows that the surface temperature increases faster than that at the mid-surface, as is expected. As a result, during the experimental time period that could vary between 1.5 and 2.5 min in the present technique, one might assume that the temperature of the pipe could increase within the range of 2 - 3 °C. The same method of recording the temperature increase was also used by Genussov (1989) and it was pointed out that the PE material could heat up 3 - 5 °C for a time lapse of 3 - 5 min. It is worth mentioning that the pipe, as a structure, has

only one-dimensional heat transfer direction, which is radial (rather than three as in the temperature measured specimen).

In order to gain a more realistic idea about the temperature rise of the pipe, two thermocouples were attached on both inner and outer surfaces and the third one was embedded on the mid-surface of a BP 002-40 180 mm SDR 17.6 pipe (10 mm wall thickness). After keeping the instrumented pipe in -15°C temperature for about one day, it was exposed to the room temperature ($\approx 25^{\circ}\text{C}$). The recorded temperature values on each three locations are plotted in Fig. 4.4.

According to the figure, the fastest section heating up is the outer surface, then inner surface and, as was expected, the mid-section is the slowest heating up. Accordingly, the temperature rise on this section in 1.5 - 2.5 min is between $3 - 5^{\circ}\text{C}$. The comparison of Fig. 4.3 with Fig. 4.4 implies that the pipe changed its temperature slightly faster than the sandwiched plates which is opposite of as it was expected. This could be explained by the use of different thermocouples, the different thickness of the samples (12 mm for plates, and 10 mm for pipe), and the exposed ambient temperature which was about 18°C when sandwiched plates was exposed (winter), and 25°C when the measurements were taken for the pipe.

4.6. Resistance to Crack Initiation: Charpy Testing

4.6.1. General Background

The Charpy test is the oldest standardized impact test in general use, ASTM E23 - 33T. Although there are several specimen designs, the most commonly used today is the 10 mm square specimen with a 2 mm deep notch having a 0.25 mm root radius. The Charpy test has served well for comparing certain materials, but translation of Charpy values is on a case-by- case basis. The test method gives qualitative, rather than quantitative information on a material's resistance to crack propagation.

As mentioned above, the Charpy test has been widely used in practice. The Charpy energy criterion provided the first correlation between the results of a laboratory fracture test and the service performance of a material in a structure. However, the energy per unit area absorbed by a Charpy specimen generally overestimates the material's resistance to fracture, R . This is due to the small ligament size of the Charpy specimen. In particular, the thickness of a Charpy specimen does not properly represent the actual pipe wall thickness. Details of testing procedure and specimen preparation for the Charpy test for steels can be obtained from Perrin (1978).

The Charpy tests for its popularity and widespread use, especially in materials data sheets, has long been recognised as being far from ideal when it comes to correlating the results it gives to real service performance. The factors comprising 'fracture toughness' as measured by the Charpy test can be analysed as follows:

- (a) Energy to initiate fracture of specimen.
- (b) Energy to propagate fracture across specimen.
- (c) Energy to deform the specimen plastically.
- (d) Energy to throw the broken parts of the test specimen.
- (e) Energy lost through vibration of the apparatus and its base, as well as through friction.

Value (a) is probably the only one of real interest, though the addition of (b) and (c) has obvious practical significance. Value (d) is referred as the 'toss-factor' which is estimated by replacing the broken parts of the specimen tested on the anvil and striking it a second time. Subtracting the 'toss-factor' from the 'impact energy' gives a magnitude corresponding the resistance of the material to the fracture. In addition to the above factors; in introducing notches, the state of cutter and actual cutting technique used has considerable influence on the results.

Since, in the propagation stage crack velocity varies continually, the Charpy energy represents some unknown average absorbed energy for a material with crack speed-dependent fracture toughness. The length of the crack travel in the Charpy test is so short that transient or free-surface effects may predominate. The critique of the application of the Charpy tests on service performance of thermoplastics and also the effect of specimen dimensions, notch tip radius, test temperature and fabrication conditions were summed up by Vincent (1971).

4.6.2. Application to Ductile Polymers

It has already been pointed out that the crack resistance R may depend strongly on such parameters as temperature, strain rate, section thickness etc. At room temperature and low strain rates, HDPE is so tough that LEFM cannot be used to evaluate it, since the material separates by ductile tearing, or simply yields across the ligament, rather than cracking with limited plasticity. 'J' testing techniques (Hashemi & Williams, 1986) yield values of 15 kJm^{-2} for two pipe-grade HDPE's even at -60°C , a temperature at which the modulus is comparable to that at impact speeds, and the yield stress is about 45 MPa. Practical methods of measuring R (or K_{IC}) rely on some means of suppressing or localising plastic flow to the point at which brittle cracking can occur; the use of stress-cracking agents to do this has already been discussed. Cooling to cryogenic temperatures can be equally effective, but one must again question the degree to which a ranking of competitive materials made on this basis will truly represent their performance in service, with different embrittling factors.

The most widely used impact toughness measuring method remains the Charpy test. This was not conceived as a fracture mechanics test, but its familiarity and the degree of its standardisation has motivated efforts to interpret its results in LEFM terms.

The Battelle group (Prabhat *et al.* 1983) used the simplest approach in interpreting a Charpy test result as a crack resistance, *i.e.*, dividing the total energy absorbed by the ligament area. Standard V-notched specimens, slightly modified to ensure a razor-sharp ($2.5 \mu\text{m}$ tip radius)

initial notch and reduce initiation resistance, were machined from the pipe wall. The fracture resistance of several HDPE pipe grades proved to be extremely temperature sensitive, with a transition very near typical operating conditions from 1 kJm^{-2} or less (comparable with a glassy thermoplastic such as PMMA) up to the 5 to 7 kJm^{-2} more usually expected from this material.

A pioneering quasi-static approach to LEFM analysis (Greig & Smith, 1973) yielded the so-called $BD\phi$ method for measuring R by Charpy testing specimens at various initial notch depths; WRc Engineering (1984) have become the first to specify this method for quality control of cold potable water distribution pipe. This specification permits a minimum R of 5.4 kJm^{-2} at 23°C for at least one of the three pipe size ranges 90-180, 225-315 and 355-500 mm. Gas-pressurised SDR 11 pipes satisfying this criterion have indeed proved to resist RCP at diameters of up to 400 mm (Gaube & Müller, 1980), so that for water distribution this would seem to be a conservative requirement. However, it is also well known that Charpy impact data for HDPE shows pronounced thickness and temperature effects.

Battelle considered but rejected the necessity to correct for thickness effects, having observed little evidence of plane-stress fracture (*i.e.* shear lips). However, Youd & Marshall (1986), and Marshall & Harry (1990) have used the *law of mixtures* analysis proposed by Williams (1984) to rationalise data from Charpy tests on HDPE pipe grades in thicknesses from 2.5 to 45 mm and with a variety of depth and sidegroove dimensions. This assumes two limiting upper (plane stress) and lower (plane strain) crack resistance properties. The results emphasise the pronounced geometry-dependence of Charpy R values, and the care needed in interpretation if an absolute material property rather than an abstract index of comparison is sought.

Although the Charpy test was originally developed for steels, Plati & Williams (1975) have shown that it can also be used to obtain the fracture toughness of polymers. They have also shown that the toughness of polymers varies inversely with the thickness of the specimen

within a limited range of thicknesses. From this pioneering quasi-static approach to LEFM analysis the so-called $BD\phi$ method for measuring R by the Charpy testing was developed. A linear relationship exists between the fracture energy and the product $BD\phi$, in which D is the thickness of the specimen, B is the width and ϕ is a shape coefficient incorporating the depth of the artificial notch.

Krishnaswamy *et al.* (1986) of Battelle proposed a modified Charpy test method to measure the Charpy impact-energy density, C_v . Specimen dimensions for the Charpy test are identical to those conducted on metals, with one exception: Instead of using a machined, *i.e.* blunt, V-notch, as in the standard Charpy test, a razor-tipped wedge with a tip radius of 2.5 μm was introduced into the specimen to a depth of 2 mm to reduce the initiation resistance of PE pipe materials. But, in using such a sharp notch, the energy required to start the crack growing from the notch is minimised. The total energy absorbed by the specimen was divided by the remaining ligament area to evaluate the fracture resistance of the material, C_v . They determined that a portion of the apparent fracture energy is expended in moving the specimen mass through the machine. It was accepted that this 'throw-away' energy was about 0.57 kJm^{-2} for a full-size specimen cut from PE pipe wall. Subtracting the throw away energy from the absorbed energy and dividing by the ligament area gives the Charpy energy density, C_v .

Although among the conventional fracture toughness tests, the Charpy test seems to be successful in yielding data for RCP in PE grades, the test method has some disadvantages. The specimen geometry and dimensions are not similar to that of the structure (normally much smaller). This size restriction introduces some inaccuracy in interpreting the test results. The reflection of multiple stress waves from the sample boundaries affects the RCP phenomenon. The inspection of the fracture surfaces of impacted samples reveals the non-uniform nature of the fracture surfaces: In the Charpy test the main characteristics of the broken sample surface are a 'thumb-nail' shaped initiation zone, arrest lines marking the curved shape of the tunneling crack tip, and the plastically deformed 'tear-lips' along the side of the sample. When the surfaces of rapidly propagated PE gas pipe grades are compared to

those of the Charpy test, some major discrepancies can easily be seen. By machining sharp grooves into the sides of the samples, the plasticity and tear-lips regions can be reduced and a brittle failure mode can be encouraged across the whole sample section. But it has been widely accepted that the use of very deep side grooves is to be avoided, as they cause lateral crack growth so that bilateral cracking is initiated. As a result, there is a significant decrease in measured toughness.

Sample sizes used for the Charpy test have a dominating influence on the magnitude of the derived fracture toughness. The size effect of Charpy specimens on fracture energy was studied by Greig & Skiplorne (1985). It was observed that 'shear lips' formation on small specimens without side grooves produced an anomalously higher fracture energy. In addition to this they pointed out that higher impact speeds substantially reduced the size of yield zone produced at the root of the main notch and changed the appearance of the specimens fracture surface. Marshall *et al.* (1988) reported that shear yield zones on sample surfaces significantly increase the fracture toughness measurements on thin samples, where the highly ductile shear lips give rise to high resistance to cracking. It was also argued that the Charpy test sample depth also influences the results, toughness increasing with increasing sample depth. Because of these reasons, Marshall *et al.* (1988) argued that a Charpy test sample with very large dimensions simulates the event better than the conventional sized samples. For all these reasons, 12.5 mm square Charpy specimens were used by them to evaluate the fracture toughness. The measured G_c values at 23 °C on 12.5 mm square samples cut from yellow gas grade MDPE pipe material with an average density of 940 kgm⁻³ (*i.e.* BP 002-40) was 11.4 kJm⁻². The values at 20°C and 3 °C have been calculated as 9.5 and 3.8 kJm⁻², respectively. These results are shown in Table 4.4.

The impact performance of PE pipe grades was studied by several researchers. Truss (1983) carried out impact tests on 'sandwich' samples of BP 002-40 between two sheets of BP 002-50 at an impact velocity of 3.38 ms⁻¹ and measured the critical energy release rate as 6.53 kJm⁻². Using the Charpy samples of BP 002-40, Grey (1983) reported a lower bound value

Table 4.4 *The mechanical properties of two different lots of MDPE material determined by Marshall et al. (1988)*

Material	Temperature (°C)	Pipe size (mm)	Average Density (Kgm ⁻³)	G _c (kJm ⁻²)	Dynamic Modulus (MPa)
BP 002-50	23	800 SDR 17	945	11.1	–
BP 002-40	23	317 SDR 17	940	11.4	–
BP 002-40	20	–		9.5	785
BP 002-40	3	–		3.8	950

of impact energy of 7.2 kJm⁻² for 23°C. It was also reported that when side-grooves of 0.5 mm were introduced, the impact resistance decreased about 40% and gave a value of 3.1 kJm⁻².

4.7. Resistance to Crack Propagation

Implicit in the use of instrumented Charpy and used by Krishnaswamy *et al.* (1986) is the following assumption: since the material is required to arrest a running crack, and since it can be argued (Kanninen & Popelar, 1985) that deceleration and arrest of a running crack is mathematically just the reverse of initiation and acceleration, an appropriate value of R for use in any propagation model is that for crack initiation.

Three comments can be made:

- a) The assumption that arrest and initiation conditions are simply related may be unsound.
- b) The available crack propagation models are derived from steady-state analysis and are appropriate to neither of these conditions.
- c) In order to approach crack arrest the system has first to sustain deceleration from a very high continuous crack velocity, through intermediate velocities at which propagation may be stable.

These amount to a powerful argument for considering the material's resistance to crack propagation across a spectrum of velocities up to the highest attained in field tests.

The impact velocity for pendulum or drop-weight Charpy test is normally within the range 1-5 ms⁻¹, imposing a strain rate of about 50 s⁻¹. Direct measurement of the crack velocity, which remains uncontrolled, demands sophisticated instrumentation and there is no established technique for doing so. This technique may, even if its results genuinely reflect *R* for the propagation phase rather than only for initiation, refer to an inappropriately low crack speed. Williams & Adams (1987) developed a sophisticated 'mass-spring-model' to analyse the Charpy impact tests. A recently developed technique for measuring *R* at a constant, and to some degree controllable, velocity is the High Speed Double Torsion Test (Leevers & Williams, 1987). Tests on 90 by 180 mm sidegrooved flat plate specimens of two pipe grade HDPE's generated crack velocities of over 200 ms⁻¹ using impact velocities of about 20 ms⁻¹. The method generates scatter comparable to that from conventional Charpy tests, but points to much lower propagation *R* values, of about 3 kJm⁻². This remains fairly constant for crack velocities above 80 ms⁻¹ but is much higher at lower velocities. The markedly curved crack front, providing a better direct simulation of tunnelling in a pipe wall (see Chapter 7), may in itself provide sufficient extra constraint to account for the lower value.

4.8. Impact Energy Measurements on PE pipe grades

4.8.1. Specimen Preparation and Experimental Procedure

To determine the fracture resistance of materials used in the experiments, the instrumented impact Charpy tests were carried out on two lots of material; namely BP 002-40 and TUB 125 pipe grade PEs which were supplied by Stewarts & Lloyds Plastics. The advantage of using instrumented impact test over the conventional Charpy test is that during the impact process more information could be obtained about the way the material behaves under the given dynamic loading conditions. Thus instrumented impact test could be a useful tool to

determine the dynamic response of the material. In addition to this, it could be possible to define the impact energy required to initiate and propagate a crack through the specimen and the manner in which the energy is consumed to create the new fracture surfaces.

It has been pointed out by Vincent (1974) that it could be seriously misleading to test an article machined from a compression molding, when the final part is to be injection-moulded or extruded. As a result, in order to take into account the whole microstructural effects, *i.e.* crystallinity profile and orientation (but not the residual stress effect) all the impact test samples were taken from the pipe wall. The orientation and dimensions of the specimens are shown in Fig. 4.5. For each temperature range three or four specimens were tested. Using a razor blade a sharp notch was introduced into the each specimen to a depth of 2 mm. In the experiments ; the thickness (B), length (L), and depth of the specimen were 5 mm, 55 mm, and 10 mm, respectively. The span between the supports was 40 mm.

The experiments were performed using a strain-gauge force transducer instrumented impact test machine produced by Compagnia Europea Apparecchi Scientifici Torino (CEAST), Italy, model AFS/MK3.

In the second series of experiments, using a razor blade, a sharp notch of 0.5 mm was introduced on both sides of samples. The mass and speed of the swinging hammer were 2.193 kg and 1.62 ms^{-1} , respectively. For higher impact speeds, it was recognised that the force-time trace of the test yielded larger transient modes and increased bouncing at specimen's contact point with the supports and striker. The same effects were observed by Dear (1990) in his recently published work. It was also pointed out, that for tough polymers, determining the fracture resistance was not always meaningful if the impact velocity was very high.

4.8.2. Determination of the Charpy Impact Energy

In analysing the results the method of Plati & Williams (1975) was used. In doing so, for the specimen dimensions used, the Charpy calibration factor, ϕ , was selected as 0.468 from

Williams (1984). The total energy value consumed by the fracture process, W , was calculated by a micro-computer connected to the impact machine. Following Plati and Williams the kinetic energy, E_k , of two halves of the broken specimen was calculated as;

$$E_k = (1 - e) \left[1 + \frac{1}{2} \frac{m}{M} (1 - e) \right] e \left(\frac{M}{m + M} \right) v^2 \quad (4.1)$$

where m is the total mass of the specimen, M the mass of the pendulum, v is the impact velocity of the hammer and e is a coefficient with the value of 0.58.

The energy going to the fracture process is given as:

$$W' = W - E_k \quad (4.2)$$

According to Plati and Williams, as the value of the fracture energy, W' , obtained for different notch depth is plotted as a function of $BD\phi$, a plot of straight line is obtained and the slope of this line gives the impact energy strength of the material. Thus,

$$G_c = \frac{W'}{BD\phi} \quad (4.3)$$

From the experiments it become apparent that there was no unique force-time (or energy-time response) but varied with the test temperature as well as side-grooving. Decreasing the temperature and/or having side grooves on the specimens resulted in a brittle type failure. On the contrary, higher temperatures have clearly produced ductile failure. Typical force-time records for ductile and brittle failure are plotted in Figs. 4.6 and 4.7, respectively.

The variation of the Charpy impact energy as a function of temperature for BP 002-40 is depicted in Fig. 4.8. As can be seen the Charpy impact energy increases as temperature is increased, but this increase becomes more significant above 0 °C, taking a value of 14 kJm⁻² at 15°C. This variation of impact energy with temperature could, to some extent, be seen in actual pipe experimental results in that the critical pressure increases moderately as the temperature is increased from -15 °C to 0°C but as the temperature is increased further the critical pressure values increase sharply. Fig. 4.8 also shows that the impact energy at 10°C is around 12.5 kJm⁻² which is only 1.38 times higher than that at -15°C. Despite this fact,

as it is pointed out in the previous chapter, the ratio of the critical pressure at -15°C to that at 10°C is about 4.88 for the BP 002-40 180 SDR 17.6.

Similar experiments were also performed on the same grade of samples but side-grooved. A 0.25 mm side groove was introduced on each side of the samples by a sharp razor blade. All the other dimensions and test conditions were kept the same. The evaluated Charpy impact energy results of this investigation is shown in Fig. 4.9. As seen, the general trend of the impact energy variation with temperature is similar to that observed in plane sided experiments. On the contrary, the significant difference here is the lower value of the impact energy. The Charpy impact energy has only risen from 4.5 kJm^{-2} to 9.5 kJm^{-2} when the temperature was increased from -20°C to 15°C .

Following the same experimental procedure tests were carried out on TUB 125. Fig. 4.10 shows the Charpy impact energy variation as function of temperature for plane samples. The first feature of the results is the high magnitude of the impact energy that varied between 11 kJm^{-2} up to 24 kJm^{-2} for -70°C and 20°C , respectively. The second important outcome of the results is the fact that the impact energy varied almost linearly within the considered test temperature range.

As with the BP 002-40 MDPE material a series of experiments was carried out on side-grooved TUB 125 HDPE. The variation of impact energy as a function of test temperature is plotted in Fig. 4.11. There are several points to pay attention to in the figure. These include the impact energy does not vary with temperature, scattering between 9 kJm^{-2} and 11.5 kJm^{-2} , which is rather surprising, and the average energy is about the minimum value that is observed on plane sided experiments.

As it has been pointed out above, the instrumented Charpy tests could enable one to have a rough idea about how the total energy is consumed in fracture process, *i.e.* the crack initiation and propagation energy. If the value of the fracture energy at the time when the force attains its peak value in force-time diagram is considered in Eq. 4.3, one can calculate the crack initiation resistance of the sample for a given test condition. Accordingly, for the

materials used in the Charpy experiments, the initiation resistances were also calculated and the results are given in Figs. 4.12 and 4.13 for BP 002-40 and TUB 125, respectively. The comparison of Figs. 4.8 and 4.13 reveals that the crack initiation energy and total energy for lower temperatures close to each other, but for higher temperatures, the total energy is almost double the initiation energy. If the same comparison for TUB 125 is made, from Figs. 4.10 and 4.13, the noticeable effect becomes clear. That is; the specimen's crack initiation energy is not so much temperature dependent and has a less scattered value of about 11 kJm^{-2} . This observation supports the conclusion of Vincent (1971) that the change from brittle to ductile behaviour becomes much less steep as the specimen density increases.

4.8.3. Discussion of the Results

It is the general feeling that the design engineer might evaluate the p_c values of a gas or water pipeline to sustain the RCP by a simple fracture toughness tests, such as the Charpy test. In almost every proceedings of the two major conferences on plastic pipelines, Plastic Pipes (U.K) and Plastic Fuel Gas Pipe (U.S.A), at least one or two research works could be found seeking a sound correlation between Charpy test results and either SS tests, or FS test results.

In the present study, the problem was considered from different perspective by carrying out a series of Charpy experiments on the two PE pipe grades, BP 002-40 and TUB 125, for a wide range of temperatures varying between -70 to 20°C . Since the RCP tests were carried out on both TUB 125 and BP 002-40 180 SDR 11 pipe at a temperature range of -30 to 15°C , these Charpy test results could be used to check whether there is either any qualitative or quantitative correlation between these two results.

It has to be pointed out that, since the present results were evaluated from a single data point on $\text{BD}\Phi$ axis, plasticity could introduce significant errors on the Charpy results.

Wilson (1989) performed two series of Charpy impact tests on TUB 125 at -20 °C and 23 °C. In the experiments the thickness and width of the specimens were about 8.5 mm and 13 mm, respectively. His calculated impact energy for -20 °C is 13.36 kJm⁻² and for 23°C is 30.34 kJm⁻². The discrepancy between these and present results on the same grade was not well understood but it may be due to the difference between the dimensions and impact speed that were considered in those tests.

Post-mortem investigation of the fracture surface of the Charpy specimens could be used as an effective tool to investigate the relevance of the fracture process in both pipe and Charpy cases. The variation of fracture surfaces of the Charpy specimens with the test temperature for the two pipe lots and different test conditions are given in Figs. 4.14 to 4.17. From these pictures, the most striking impression is the distinctly different regions forming the fracture surfaces. Particularly, the increasing ductility with temperature is very apparent for both BP 002-40 and TUB 125 plane sided PE. But this increase in ductility becomes almost unchanged for the side-grooved tests, particularly for TUB 125 as seen in Fig. 4.17.

4.9. Conclusions

It is clear from the Charpy impact energy results that there is no simple general theoretical or empirical relation between the Charpy test results and measured critical pressure values. Any relation could only be a forced correlation, *i.e.* valid for a very narrow temperature range. Also, considering the the effect of temperature on critical pressure for each grade, this effect cannot be seen in the Charpy test results. Particularly, a rather sharp rise in the critical pressure above a certain temperature, which is believed to be a characteristic material property, could not be explained by the present Charpy energy results.

The Charpy energy is measured for a reduced thickness specimen. Thus, the amount of material deformed in Charpy test, *i.e.* tear lips, shear lips, is much more higher than that in an actual pipe fracture. The plastic zone size in the Charpy test is different from the that seen in RCP broken PE pipes which is an important parameter for the material resistance. Thus,

the Charpy tests could only characterise the specimen's impact energy but not the material's. Additionally, the Charpy energy is the sum different contributions from crack initiation, propagation and other terms, like loss of energy by plastic deformation of the specimen on the supports and on the hammer. It is rather difficult to identify a uniquely relevant crack propagation energy from this sum. Moreover, as the crack history was recently monitored by Dear (1990) using high speed photography, the crack velocity varies during the impact test and different material resistances are integrated in an uncontrolled manner. As a result, there is an urgent need for a better test to evaluate the resistance of ductile polymers to brittle fracture. Despite these criticisms, it is generally felt that no fracture mechanics test offers as much information for such a small expense as the Charpy test.

From the results of present investigation, especially those of Figs. 4.8, 4.9, 4.10, and 4.11, one may suggest that introducing the side-groove on the specimen eliminates any tear-lips at each side of the sample, and thus promotes plane-strain fracture. The plane-strain fracture toughnesses of the PE investigated are not as strongly temperature dependent as expected.

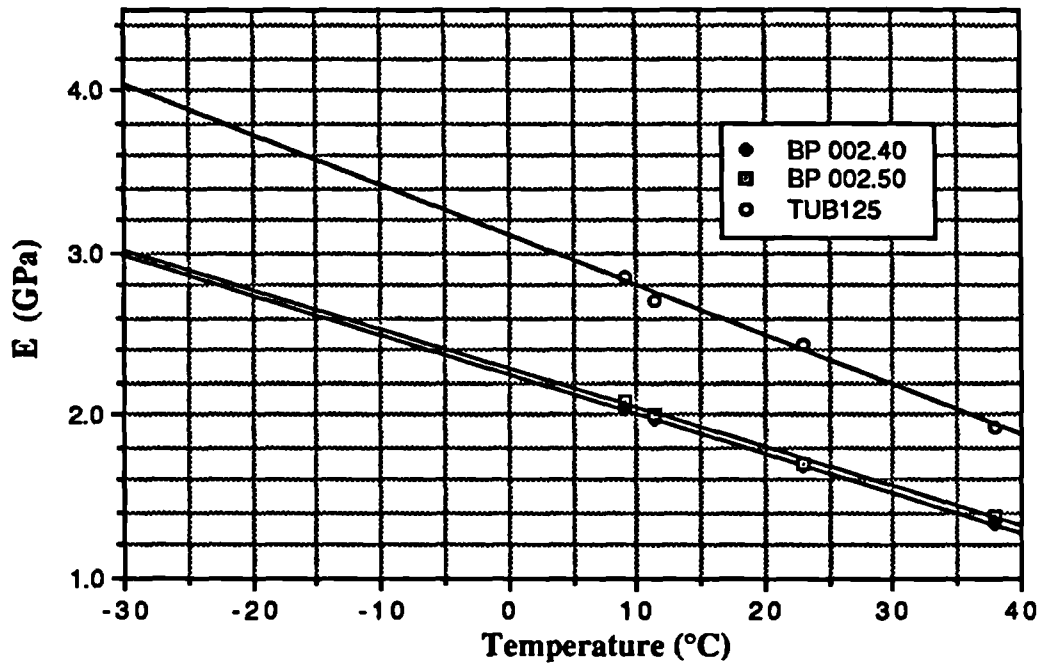


Figure 4.1 Effect of temperature on the dynamic Young's moduli of the three different pipe grades PE (Dioh, 1989).

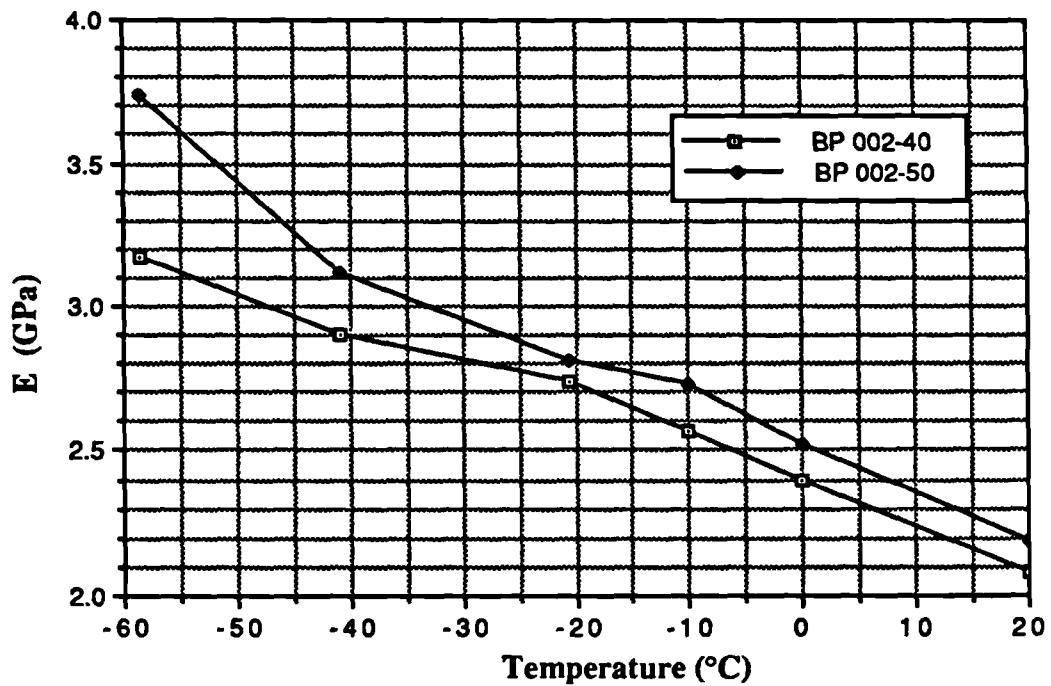


Figure 4.2 Effect of temperature on the dynamic Young's moduli of the two different PE pipe grades (Crouch, 1986).

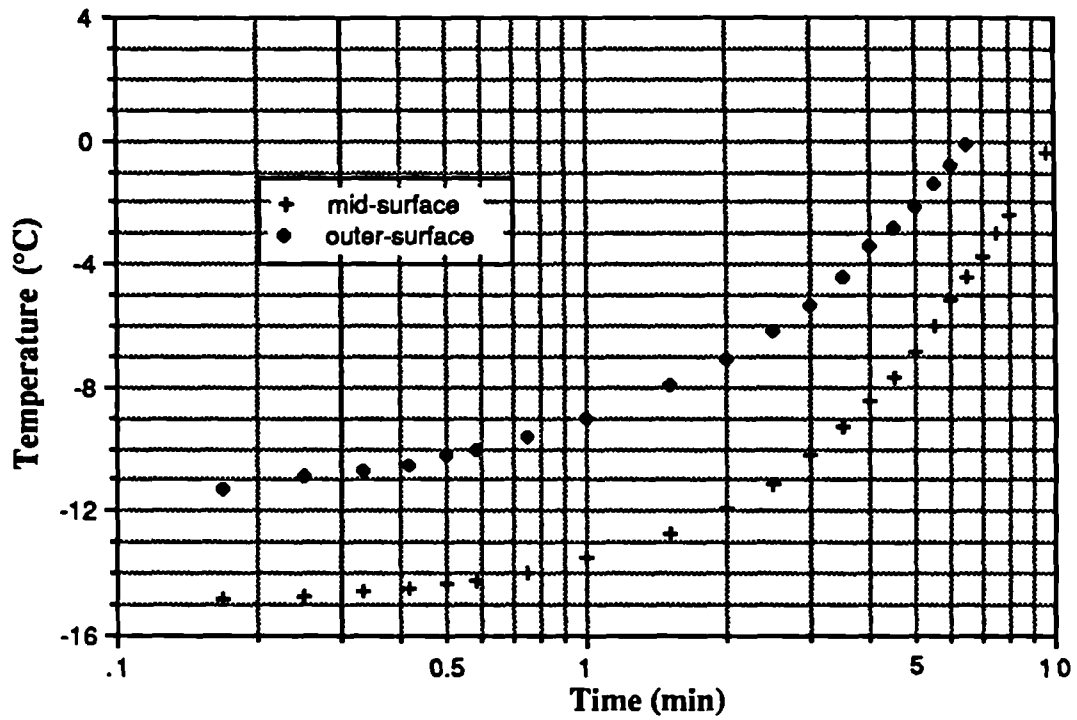


Figure 4.3 Temperature - time response of BP 002-40 PE pipe grade sandwiched plates exposed from -15 °C to room temperature.

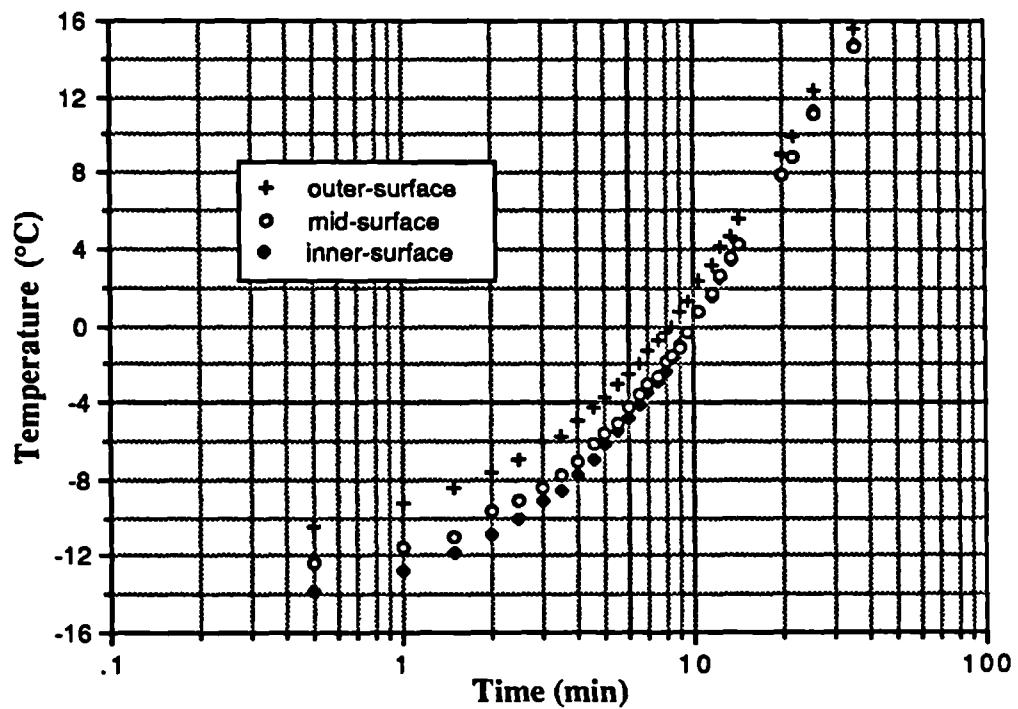


Figure 4.4 Temperature - time response of BP 002-40 PE 180 mm SDR 17.6 pipe exposed from -15 °C to room temperature.

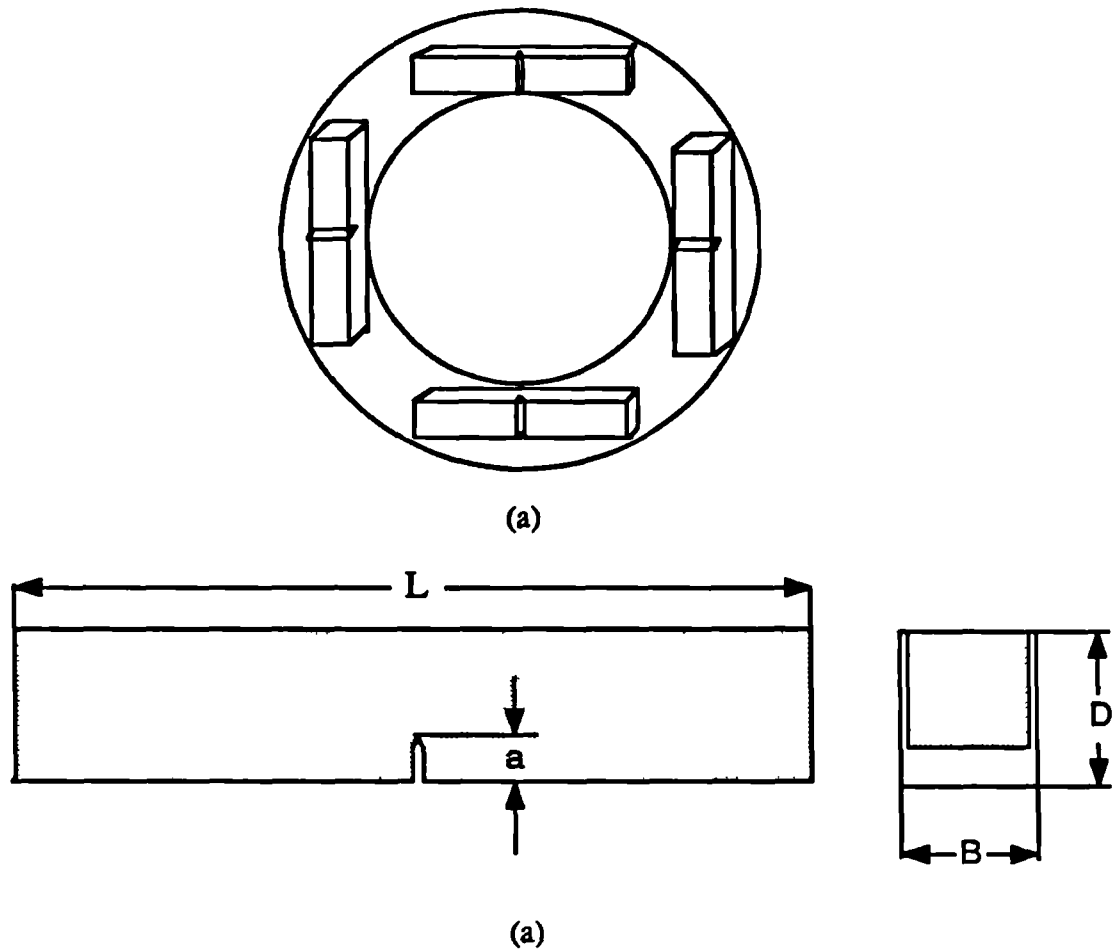


Figure 4.5 Charpy test specimen configuration and dimensions.

TEST N. UTUBRM4

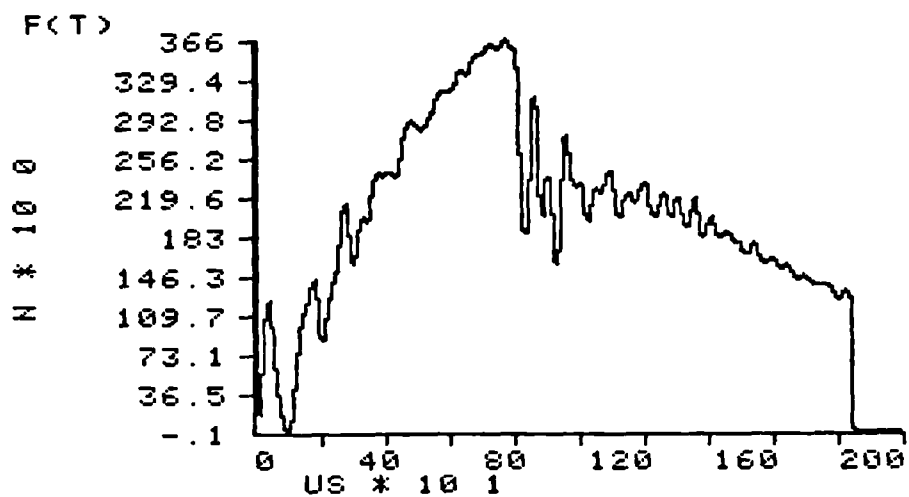


Figure 4.6 Typical ductile fracture force-time recording (TUB 125, tested at 20 °C, plane specimen).

TEST N. UTUB-60/2

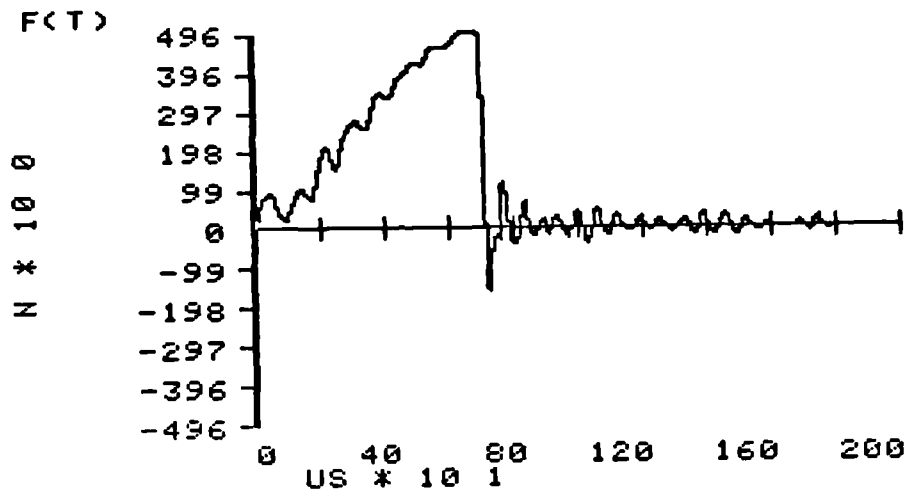


Figure 4.7 Typical brittle fracture force-time recording (TUB 125 tested at -60°C , plane specimen)

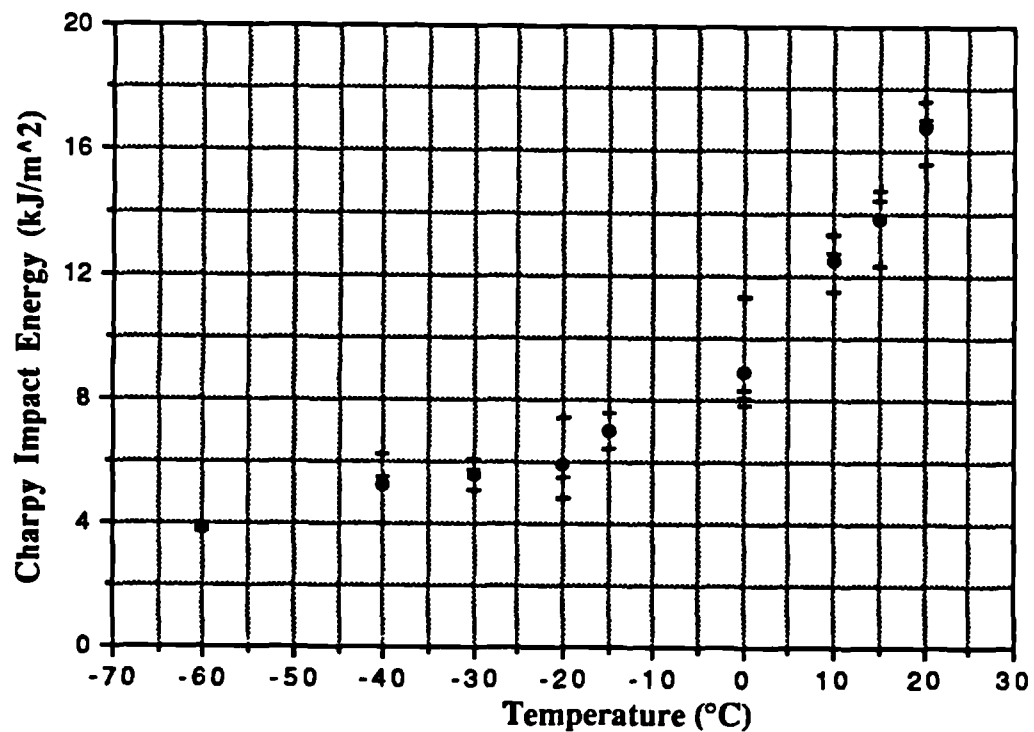


Figure 4.8 Variation of the Charpy impact energy with temperature for plane sided BP Rigidex 002-40 MDPE material.

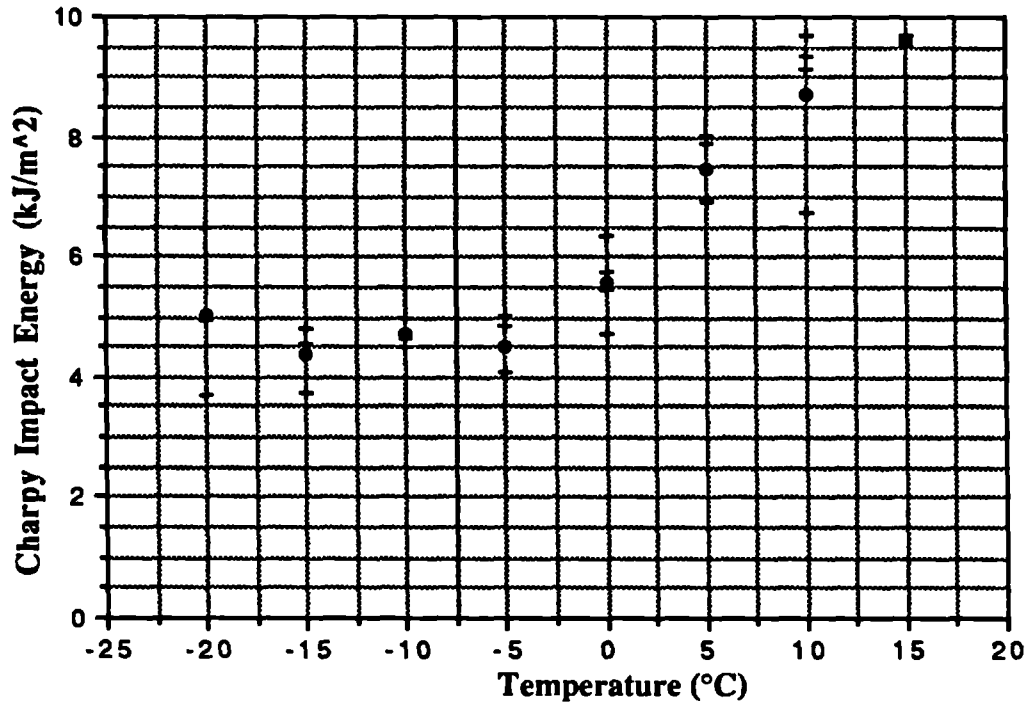


Figure 4.9 Variation of the Charpy impact energy with temperature for side-grooved BP Rigidex 002-40 MDPE material.

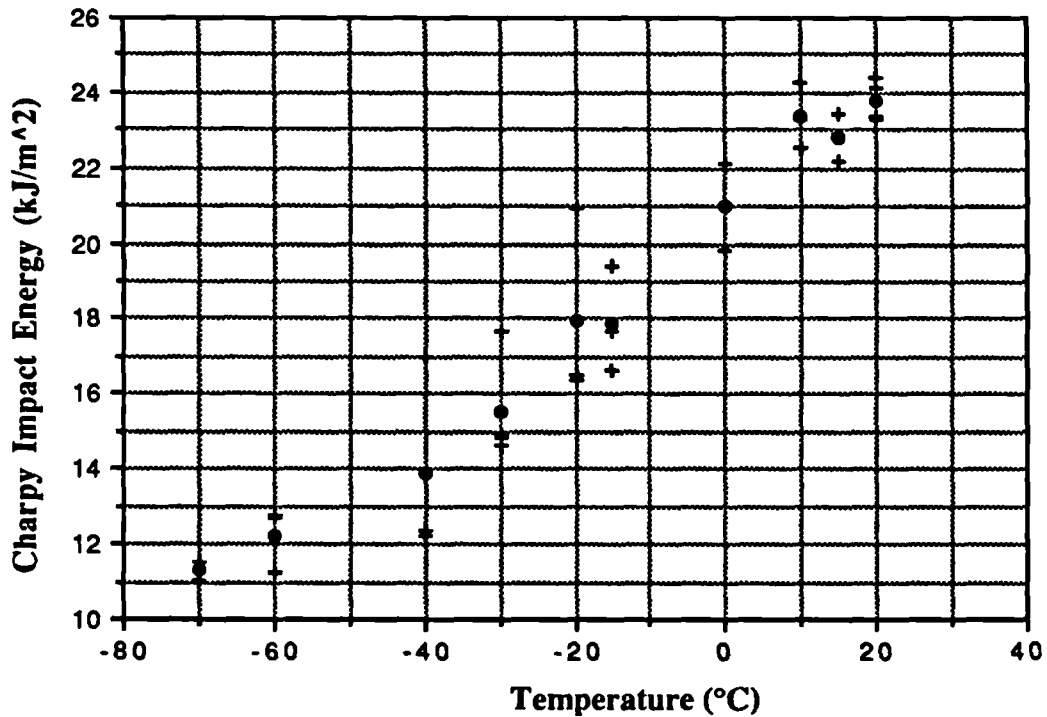


Figure 4.10 Charpy energy density of plane sided TUB 125 (orange) gas grade PE material as a function of temperature.

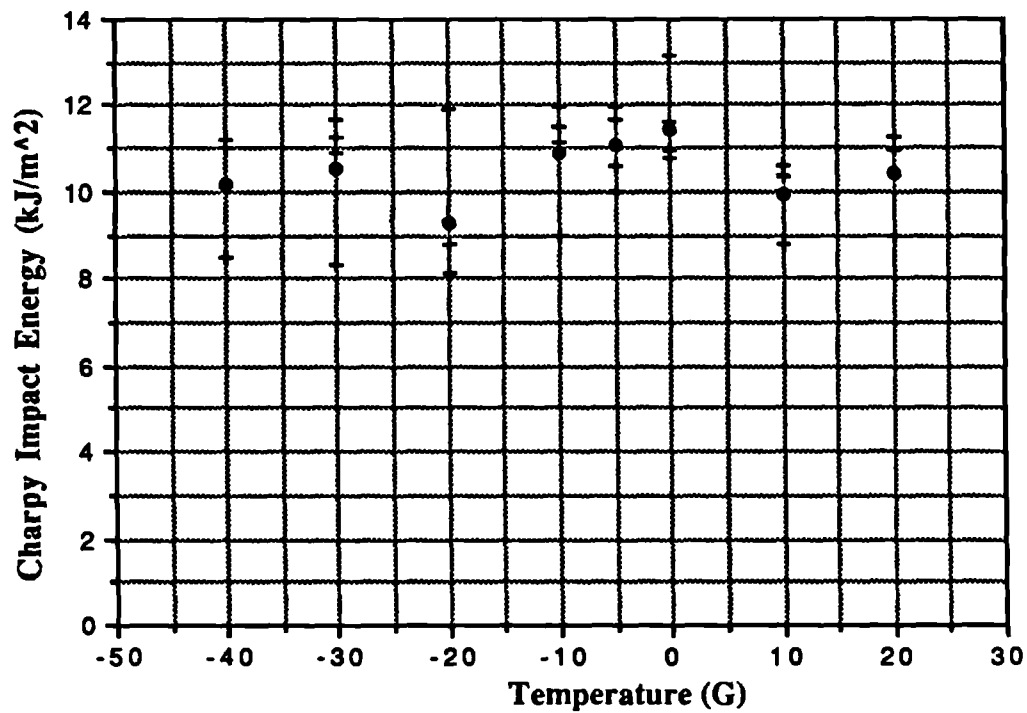


Figure 4.11 Charpy energy density of side-grooved TUB 125 (orange) gas grade PE material as a function of temperature.

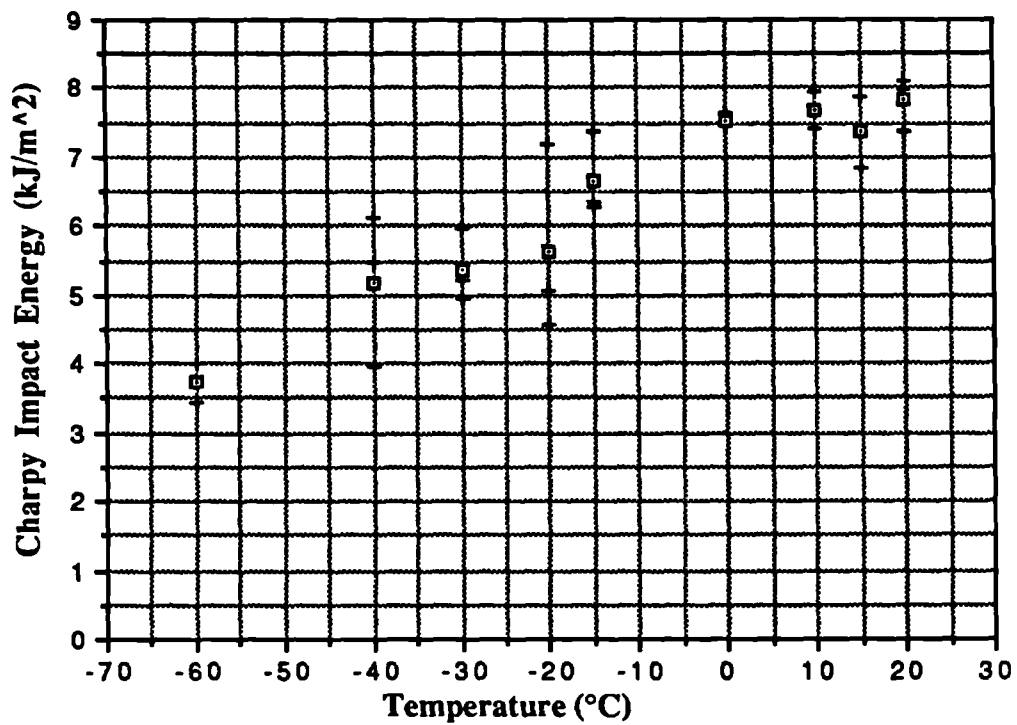


Figure 4.12 The Charpy crack initiation resistance of plane sided BP 002-40 material for different test temperature

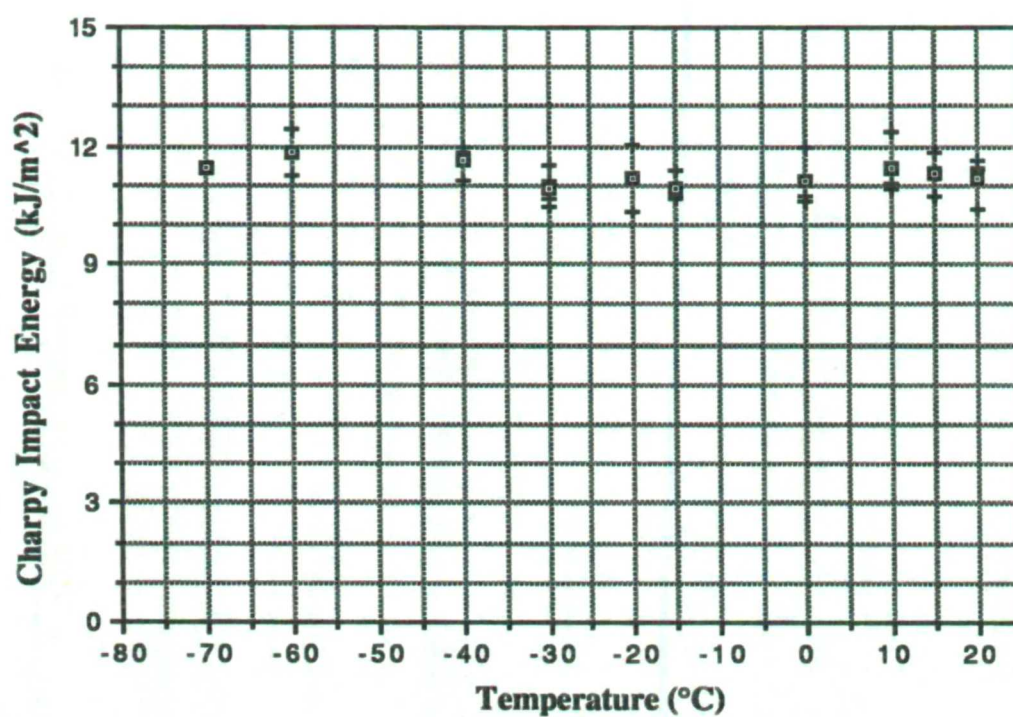


Figure 4.13 The Charpy crack initiation resistance of plane sided TUB 125 material for different test temperatures.

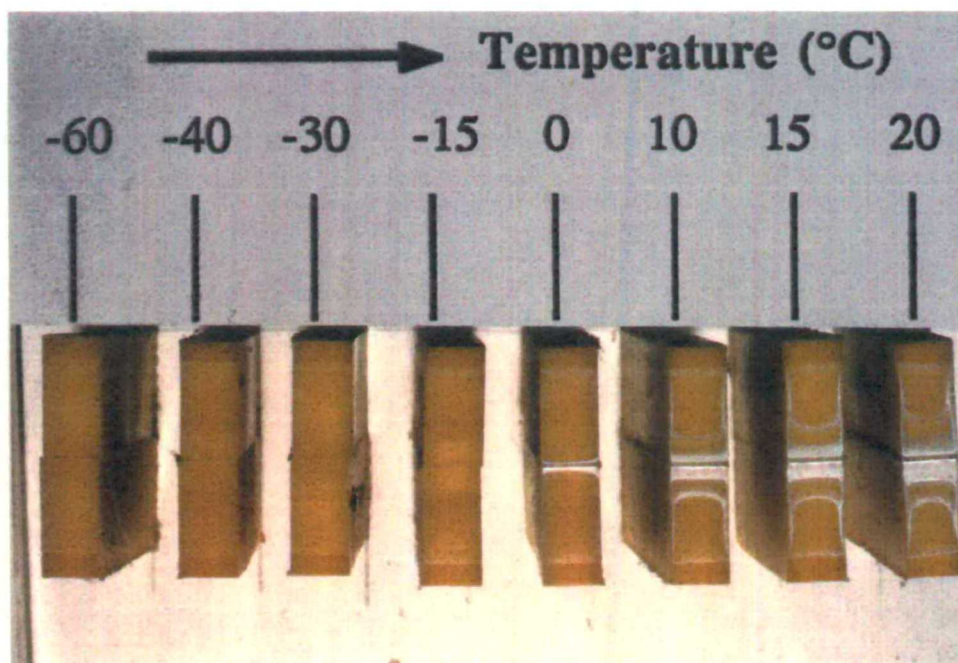


Figure 4.14 Variation of fracture surface ductility with temperature for plane sided BP 002-40 pipe grade PE.

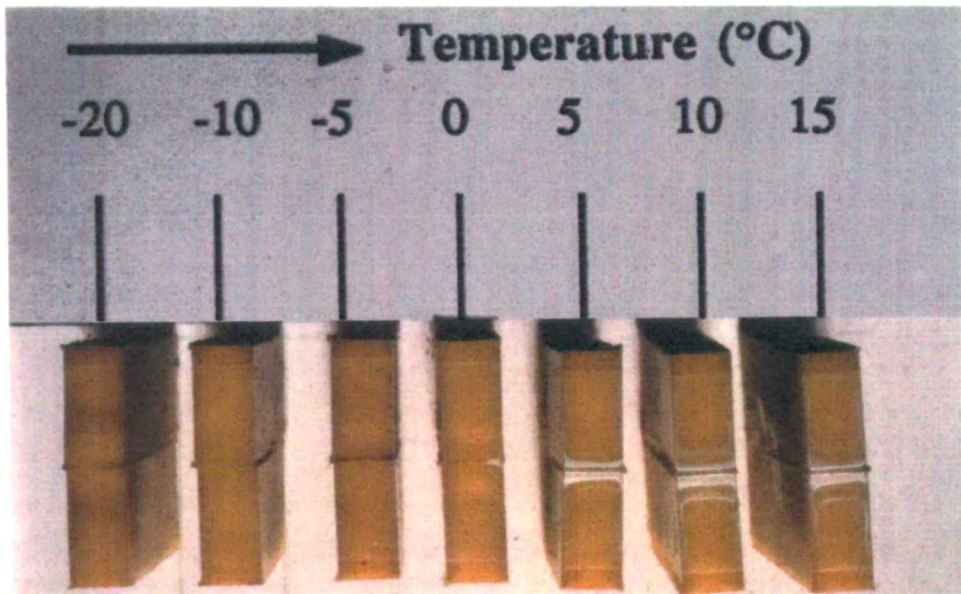


Figure 4.15 Variation of fracture surface ductility with temperature for side grooved BP 002-40 pipe grade PE.

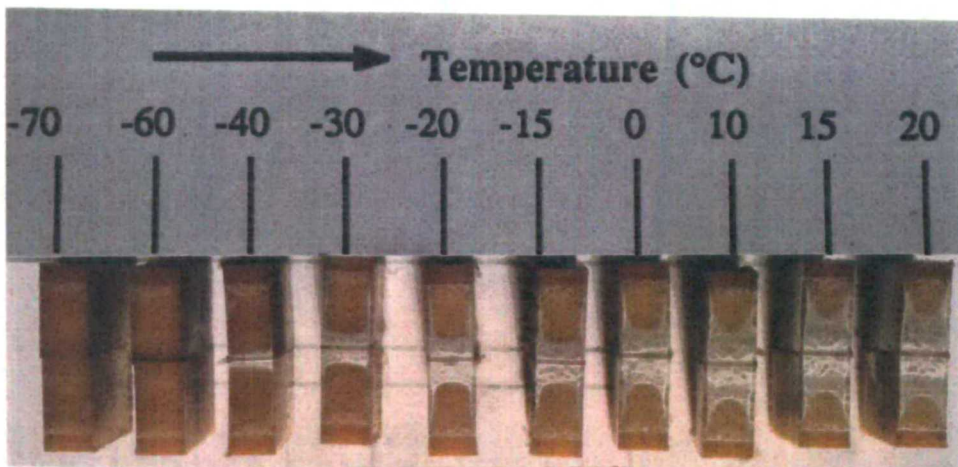


Figure 4.16 Variation of fracture surface ductility with temperature for plane sided TUB 125 pipe grade PE.

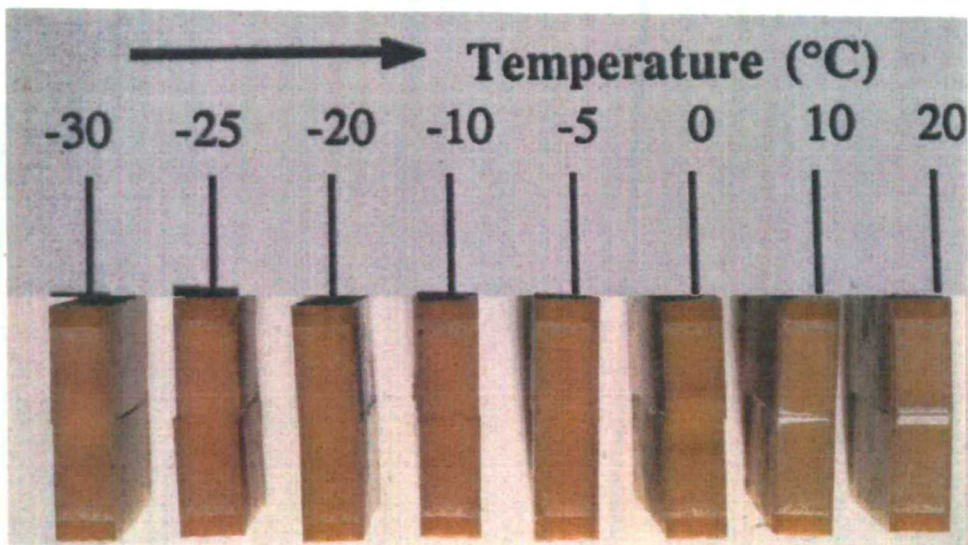


Figure 4.17 Variation of fracture surface ductility with temperature for sidegrooved TUB 125 pipe grade PE.

CHAPTER 5

EXPERIMENTAL RESEARCH DESIGN AND PROCEDURE

5.1. Introduction

To define the safe and allowable operating pressure for pipelines, full-scale field tests have been carried out by a number of research centres all over the world; the main ones are British Gas of UK, Battelle Memorial Institute (BMI) and Southwest Research Institute (SwRI) of USA. Although these tests are the most effective and accurate way of defining Rapid Crack Propagation (RCP) performance of materials, they are not only expensive to carry out but also laborious. Apart from these, no conclusive and reliable design procedure has been obtained. One of the main reasons for the lack of such a design procedure is that in order to apply fracture mechanics concepts to define the critical pressure, the dynamic fracture resistance and elastic modulus of the material should be characterised precisely. Another difficulty is that a plastic pipe has manufacturing process dependent properties and these may not be characterised completely by a small specimen. Consequently, to define the RCP performance of the structure, the pipe itself has to be tested directly to take into account the structural and lot to lot variation.

5.2. Developing a Novel Test Technique

In the developing of a new small scale test to study RCP in plastic pipes, the main aim is to observe what the behaviour of an initiated brittle fast crack will be. The test rig has to be designed in such a way that whether the initiated crack propagates to the end of test pipe or stops, decompression in front of the crack tip should be suppressed. This precondition is

very important in small scale (SS) tests in that if it is not satisfied, a very long test length, of around 10 m, is required to attain a steady state gas dynamic in front of the crack tip. For smaller test lengths the pressure during crack propagation changes with time so that the results will be inconclusive *i.e.*, when the crack stops, it is impossible to decide whether this is due to lower initial pressure or continuously decaying crack tip pressure. In previous SS test techniques, such as Modified Robertson test developed by Vancrombrugge (1982), the supply of gas to the crack tip decays rapidly as the crack propagates. Moreover, the reflection of the decompression wave from the end of a short pipe causes a sudden decrease of pressure at the crack tip. Consequently, this SS test is unlikely to be an efficient tool to study RCP or to define the critical pressure for a pipe under required test conditions.

Thus, the objectives of developing a new small-scale test were:

- i) The prevention of transient decompression by radial gas flow from unrestrained flaring of the pipe.
- ii) The suppression of axial decompression wave propagation.
- iii) The effective isolation of the transient initiation event from the subsequent steady state propagation process.

It has to be emphasised that although the objectives above are essential for reliable SS RCP testing, in a FS test none of them are necessary.

Beside the above objectives, particular effort was put into minimising specimen preparation procedures, such as the super-cooling and special machining of the initiation zone which are used in the 'Modified Robertson' (MR) test. In all the tests the pipe end requires chamfering: the removal of material from the inside of the one end to allow pipe to cross the pressure 'O' rings and to locate it on the rig.

In the MR test, at each end of pipe specimen, an expendable rubber end-plug has been used. Since these end-plugs were not connected each other in order to prevent the axial extension of the test pipe, an axial stress of half the hoop stress is introduced into the test specimen as the

pipe is pressurised. Since a pipeline system can be assumed to be infinitely long, having such a high axial stress in a test section does not reflect the actual loading condition. This problem was eliminated in the present technique by using a central core both to locate the baffles and to fix the end seals. Although these end seals are pressure actuated, they need to be pre-tightened before the start of pressurisation. Having such a pre-tightening obviously introduces a certain amount of circumferential stress in the pipe at the seal locations. This effect was minimised by loosening the compression nut after pressurisation.

Bearing in mind the objectives mentioned above, a novel SS pipe test technique has been developed and is shown schematically in Fig. 5.1.

The critical dimensions of the rig are given with respect to the pipe nominal outside diameter, D .

- The containment cage must allow free expansion of the pipe during pressurisation, but must restrict radial flaring of the fracturing pipe, at any point around its circumference, to a maximum diameter of $1.1D$.
- The normal pitch of the adjacent rings $D/3$, and their axial separation did not exceed $D/2$ at any point along the PZ length.
- The internal length of PZ was $4.5D$.
- The decompression baffles was not less than $0.96 d_{\min}$ and not more than $0.96 d_{\min}$ in diameter, where the minimum internal pipe diameter was given as;

$$d_{\min} = \left[1 - \frac{2.2}{SDR} \right] \quad (5.1)$$

- The normal baffle pitch was $D/3$ and was not greater than $D/2$.
- The internal volume of air within the PZ section, which could expand without restriction to drive the pipe wall radially and circumferentially outwards, was 20% of the total internal volume.

- The pipe wall was fully supported under the impact point by an anvil of axial length exceeding that of the blade.
- The initiation arrangement and condition were checked to ensure that a fast crack penetrated the PZ by a minimum axial length of $D/3$ and did not exceed $1.5D$ within the PZ.

The test rigs for pipes of 180 mm and 250 mm diameter are photographically shown in Figs. 5.3 and 5.4, respectively. A combined external and internal seal at the fixed end locates a specimen of total length $7D$. An internal, pressure-expanded seal divides this length into an initiation zone (IZ) of length 2 diameters, and a propagation zone (PZ) of length 5 diameters. Both seals located on the axial central core are designed so that when the pipe is pressurised they do not create any axial stress, as in actual FS pipelines. The PZ and, if required the IZ, are pressurised separately by air, and the pressures are monitored - in the PZ by a sensitive pre-calibrated strain gauge pressure transducer whose output is displayed by a digital pressure indicator having ± 0.001 bar accuracy. Air to pressurise the pipe is supplied from the workshop air system, but for a pressure higher than 5 bar, the required pressure either for the gas-gun or test itself is supplied from a rechargeable air bottle having a pressure of up to 150 bar. The pneumatic block diagram of the test rig is given in Fig. 5.4.

5.3. Crack Initiation

Initiation of a fast crack in a pressurised gas PE pipeline is not easy, but if it is possible, RCP could be triggered above a critical pressure value. Thus, a distinction has to be made between crack initiation and fast crack propagation. Consequently, one of the pre-conditions for a reliable crack propagation test is to initiate a sharp crack having a high initial crack velocity exceeding the maximum decompression velocity. Once this prerequisite is satisfied, with a reliable test technique a series of tests could be carried out to define the critical pressure for given test conditions.

In a real pipeline system there are two sources from which a fast crack can initiate:

i) RCP may initiate from a stress concentration. A poor installation of socket joints or electro-fusion welding may result in high stress concentrations. Furthermore, the axial misalignment of PE pipes at butt fusion joints could initiate cracks (Brown & Parmar, 1989). After some incubation time a slow crack may initiate and propagate from these critical regions. The other source of these stress concentrations is the weld-bead of butt welding. Moreover surface embrittlement or other material flaws (Broutman, 1981) as well as rock impingement may cause such a failure mode.

ii) RCP may be triggered by impact loadings such as penetration of a sharp object or deep excavation.

In his work, Proudman (1988) has classified the main breakages into five categories: Engineering (such as backfill and pipe bending), man-made external effects (traffic loading, deep excavation), human interference (third party), vegetation and climatic or subsoil effects.

To initiate an artificial fast crack several techniques have previously been used in RCP testing. These initiation techniques could be summarised as below:

i) Radial impact of a sharp blade to a machine grooved initiation section. This technique was used by Greig (1985-b) in his FS test. In these experiments, in order to minimise pipe deformation and vibration during fracture initiation by the impacting blade, the pipe was supported internally. However, as the following experiments failed to yield a fast initial crack, the initiation section was cooled by a mixture of solid carbon dioxide and methanol at -70°C prior to testing. The same technique was also successfully used by Gaube & Müller (1980) and Bragaw (1980) to initiate a fast crack in their PE pipes.

ii) Axial impact of a pendulum hammer to a specially machined notch that was cooled by liquid nitrogen (-196°C). Firstly, Vancrombrugge (1982) has used the technique

to induce a fast crack for his small-scale test technique. The same technique was also employed by Prabhat *et al.* (1983), Wölter & Ketel (1983), Wölter (1985), in their SS Modified Robertson tests. Prabhat *et al.* (1983) have modified the initiation technique. In their method, the previously mentioned notch was replaced with a wedge placed in a simple starter notch. The notch tip was then super-cooled and a wedge struck with a hammer hitting in the pipe axial direction.

iii) Bragaw (1980) has managed to introduce a fast initial crack with a different technique. A long defect of degraded polymer was generated by puncturing the pipe wall with a 15 cm knife having 30° tip angle, and then the puncture was filled with melted PE. Since the degraded zone was severely oxidised, a fast crack could easily initiated by impact of a sharp knife.

iv) By introducing an initial notch in PE pipe in the longitudinal direction and pressurising the pipe until the crack suddenly advances through the thickness Nussbaum (1985) has successfully initiated a fast crack. The technique is also known as 'static crack initiation'. Gaube & Müller (1980) used the same initiation technique to trigger a fast crack in their RCP experiments on PE.

v) Machining a 254 mm long notch along the pipe, 80% through the wall, sharpening this notch with a razor blade and then wrapping this section with a three layers of fibreglass tape (to prevent the notched section from deforming during pressurisation) Prabhat *et al.* (1983) has introduced a fast crack by two different methods. The first consisted of suddenly cutting the wrapped layers with a sharp knife and causing the remaining ligament to snap. The second method involved locating a shaped explosive charge over the flaw to cut the remaining ligament.

In order to initiate a fast crack almost all the techniques mentioned above require extra processing and/or machining. Moreover, initiating a fast crack by an axial impact could introduce some abnormalities on crack propagation.

During this research program, various initiation techniques have been used to initiate a fast crack into pressurised pipe. The initiation of fast propagation was found to depend on the projectile conditions. The shape of knife and the speed of the projectile are very important in initiating a fast crack.

Fast crack initiation is achieved by a gas-gun fired chisel projectile, tipped by an axially-aligned chisel blade of varying tip angle and width. The impact velocity can be adjusted by the pressure in the gas-gun. This velocity was measured as a function of gas-gun pressure, using an infra-red beam interruption system and the variation of impact velocity is shown in Fig. 5.5. The figure shows that the impact velocity is around 26 ms^{-1} for 6 bar and 37 ms^{-1} at 10 bar gas-gun pressure. This is the velocity the projectile would achieve in free-fall through 70 m. To minimise the deformation of the pipe at the impact zone and achieve higher initial crack velocities the pipe is supported by an anvil immediately below the impact point as shown in Fig. 5.6. To initiate fast cracks, various knife shapes and dimensions and gas-gun pressures were employed, Fig. 5.7. The knife is attached to the tip of a projectile of 1570 g mass.

With the pipe-rig developed, initiating the required initial fast crack can be controlled by various test parameters. These optimum parameters were found to vary from one material to another and with different temperatures. This suggests that for every material, before starting the actual 'arrest-propagation' tests, without pressurising the test section a set of calibration tests have to be carried out to make sure that under the test conditions a fast crack is successfully initiated and that this initiated crack can propagate to a minimum acceptable initial length. For our tests this was taken as to be one diameter from the beginning of the test section. If this initial crack length is too long, exceeding two diameters from the beginning of test section, the effective length of the test section becomes shorter and consequently not enough remains to define the critical pressure accurately. Thus, in all the tests the crack initiation parameters have to be adjusted so that the minimum acceptable crack length is one diameter and the maximum is two diameters in length after initiation section.

From the results of many crack initiation trials the parameters affecting the crack initiation phenomenon could be summarised as;

a) blade type: various type of blade configurations were examined for crack initiation as shown in Fig. 5.7. It was understood that the wedge type knife is the most efficient one in generating fast fracture. As far as length of the blade is concerned longer blade lengths tend to give faster and longer initial cracks. In the present study the blades used were 0.36 D in length. Thus for '180 mm rig' the length was about 65 mm and for '250 mm rig' it was 90 mm.

b) blade angle: This parameter is of particular importance. Higher blade angles cause severe plastic yielding and distortion around the impact zone and presumably decelerate the speed of projectile penetration too quickly. Additionally, higher angles reduce the final length of blade penetration into the test pipe before a fast crack triggers. On the other hand, very small blade angles did not result in the desired initial fast cracks. Initial experiments with smaller blade angles indicated that although the the final penetration length was longer, the initiated fast crack did not attain the required final length. It seems that smaller blade angles do not create the required high strains around the impact zone that are believed to be responsible for fast crack initiation. Experiments with different angles indicated that the optimum blade angle was about 25°. Having a side angle of 45° at each side of the knife was found to give better crack initiation.

c) pipe wall thickness: Increasing the wall thickness tends to produce faster initial cracks. Fracture surface examination of different pipe wall thicknesses revealed that thicker pipes have less ductility. Furthermore thinner pipes resulted in severe damage around the impact zone.

More recently, tests on different PE pipe grades have indicated that having an initial notch at the inner side of the pipe along the initiation section made it possible to initiate a fast crack on any grade of PEs over a wide range of temperatures. This notch eliminates the ductility at the inner surface and gives faster and longer initial cracks.

5.4. Crack Velocity Measurement

A knowledge of the crack velocity history is important because it is believed to represent the fracture state at the tip of a propagating crack and to determine whether the crack will arrest or propagate.

5.4.1. General Background

Several crack velocity measurement techniques have been used by various investigators to record the crack behaviour in their RCP experiments. The currently employed techniques for the measurements of crack position history, and hence the crack velocity, can be divided into four groups: optical, electrical, acoustic, and compliance methods. Another classification could be made as: continuous crack length recording as a function of time, and discrete crack length measurements as a function of time. Each of these techniques is briefly described below and the advantages and disadvantages of the techniques were also reviewed. The techniques were compared and limitation of each technique were reviewed by Stalder *et al.* (1989). The most commonly used techniques in the field of RCP to record crack history are briefly summarised below.

a) Optical or 'photographic' methods in which the position of the crack tip may be determined by direct observation of the fracture of the sample. For crack velocities higher than a few millimetres a minute, it is necessary to use some kind of external recording method such as a video recorder or a high speed camera, generally with a microscope or magnifying glass, to record the event. The crack velocity may be calculated by extrapolation between successive value of $a(t)$, where a is the crack length and t is the corresponding time. The photographic technique can be applied in two different ways : framing (discrete), and streak (continuous)

photography. In the former, the image of the crack length is recorded successively at a given rate. This could be done in two ways: the Cranz-Schardin camera, by which 8 to 16 exposures of an event can be recorded, and a rotating mirror camera that is capable of recording 20 to 200 frames at a speed of up to 2×10^7 frames per second. The technique has been used by Kobayashi (1979) and Kalthoff (1983, 1985). The accuracy of crack velocity measurement with this technique depends very much upon the distance between the length interval a . This is because the crack propagation may not be continuous (a series of run/arrest phenomena), or that there is sudden acceleration or deceleration. However the technique could give a useful estimation of the mean crack velocity. Using the high speed photographic technique Shannon & Wells (1974) studied RCP in uPVC gas pressurised pipe at up to 10^4 frame per second, and Kobayashi *et al.* (1988) recorded RCP in pressurised steel pipes for CTOA recording.

In streak photography, only a thin section of the sample is observed and the image is swept across the screen at a constant rate. The technique has been used by Kobayashi (1979) and Ravi-Chander & Knasuss (1984 a-b). In the group at IC, Genussov (1989) and Ivankovic (1991) have used the technique to record continuous crack velocity records. Although this technique is quite effective in crack velocity measurements, it may be very difficult to record an oscillating crack history. Also, a relatively large space is required at the both sides of the specimen to place the required devices. Due to these features this technique has little potential to be used for crack history recording in pressurised pipe.

b) Electrical methods: In the field of polymer research a number of investigators have used a series of conductive lines painted on the specimen surfaces across the prospective crack path using silver paint. During crack propagation, as the crack breaks the successive lines, it produces a jump in resistance. By making measurements of crack length and time corresponding to the interval between successive lines, average crack velocity can be calculated. The technique has been used by Genussov (1989) and Leever & Williams (1985) for crack velocity measurement in the HSDT specimen. During crack propagation no measurements can be obtained between each discrete line. If crack propagation is stable this

is not a serious problem, but if the crack propagation is unstable, the crack history cannot be measured with accuracy.

The technique has been successfully used by Greig (1985-b) for crack velocity recordings in full-scale PE gas pipes. Shannon & Wells (1974) have also applied the same technique to record crack propagation history in small-scale uPVC gas pipes.

c) The graphite gauge technique: Stalder *et al.* (1983, 1985 and 1989) have developed and employed a velocity gauge technique in impact testing of polymers. In the technique a suspension of graphite particles is sprayed on the surface of the samples where the initiated crack is expected to propagate, and limited by two silver line electrodes. It was reported that resistivity calibration of machined crack showed a linear correlation and crack velocities between 10^{-6} and 10^3 ms^{-1} could be measured by the technique. In order to get a reliable crack propagation history it was pointed out that the a uniform layer of conducting graphite has to be used for fabrication of the gauge, and that the silver paint lines have to be parallel. Moreover, sensitivity of the gauge to deformation taking place ahead of the crack tip could induce some uncertainties. Although this can be negligible in brittle polymers in which the plastic zone is generally about $100 \mu\text{m}$ in size, but in the case of ductile polymers with large plastic zones, the continuous reduction in resistance could be observed as a function of deflection. The presence of humidity also increases the resistance of the gauge. Hence, conditions must be maintained throughout the test.

d) Acoustic methods: Since a defect or crack provides a discontinuity of the material, it causes the reflection of waves sent through the material. As in quality control, these reflected waves could be recorded by transducer to get information about the size and location of a crack.

e) Compliance methods: By defining the compliance of the sample having particular certain dimensions, elastic modulus and crack length, it is possible to deduce the crack history. Deans & Richards (1981) have argued that the technique could be more sensitive than electrical techniques. The technique seems to be attractive expect when it is applied to certain

geometries, such as a double cantilever beam, which are too long to yield the required sensitivity. In contrast, the sensitivity of the method is low especially for shorter cracks.

An ideal method of measuring the crack velocity in PE pipes would have the following features:

- i) measurements should be possible between -30 °C up to ambient temperature;
- ii) the time required to take the pipe from freezer, locate it into the test rig and carry out the test has to be relatively short in order to reduce the temperature variation of the sample;
- iii) the technique should not interfere with the restraints on the pipe;
- iv) should allow for measurements of crack lengths in the range of 100 to 150 cm;
- v) and should be applicable to fast crack velocities, and not only straight but also oscillating crack propagation.

Among of the available techniques it can be seen that the electrical (grid line) technique has the potential to meet the above requirements. Various methods were applied to measure crack velocity in the present study.

5.4.2. Crack Velocity Measurements with Copper Wire

Copper wires having 0.15 mm diameter were embedded into the surface of the pipes in the circumferential direction for about $\pm 150^\circ$ from the initiating crack axis. The ends of the wires were then connected to a resistance circuit so that interruption of electrical continuity resulted in a change in voltage level. From the series of changes in voltage levels crack velocity can easily be measured. This information was stored in the transient memory of an oscilloscope. As the crack tip approached the wire, the wire elongated by about 3 mm to 13 mm and did not break at the same time as material separated. Thus, the actual crack velocity could not be measured and this copper wire method was abandoned.

5.4.3. Crack Velocity Measurements with High Conductivity Paint

Nickel high conductivity paint lines, 5 mm wide, were sprayed on the surface of each test pipe. The ends of the lines were connected to a network, and the output connected to an oscilloscope. Since the resistance of the conducting paint was high enough, no series resistance was used in these measurements. This method was quite effective in that it took a relatively short time to prepare the timing wires. However, since the conducting paint used was very brittle some of the wires broke due to radial elastic expansion during pressurisation and others were broken by the very high deformation of the region ahead of the crack tip rather than the actual crack. As a result reliable crack velocity measurements could not be recorded with this method.

5.4.4. Crack Tip Velocity Measurements with Silver Paint

Since using copper wire and conducting paint were not effective, some three point bending and tensile tests were carried out with flat MDPE specimens on which timing lines were painted in different directions. It was recognised that the conductive paint had broken at very small bending deformation. On the other hand silver paint did not break even at a relatively high bending deformation. In other words silver paint 'timing wire' can only be broken by a propagating crack. In most of the pipe tests, 20 silver paint timing wires were used with a width of about 4 mm. To start the recording of the voltage output a triggering line was painted at the beginning of timing wires. During crack propagation, as this triggering line is broken by the crack tip, the recording of the voltage output was started.

For all crack velocity measurements ELECTRODAG 915, a high conducting silver paint, produced by Acheson Colloids Co., was used. The timing wires were painted with a thin brush onto the pipe outer surface and, after drying, each end of the circumferentially painted lines were carried to the end of the pipe with a thin copper wire. Each copper wire was also embedded into the bottom of the pipe in the axial direction to eliminate any possibility of contact which may happen during the test. Since the conductivity of the silver paint used was

very high a set of resistors was designed to give equal voltage steps as the propagating crack broke the wires successively. The circuit diagram used for crack velocity measurements is shown in Fig. 5.9.

A Nicolet transient recorder, model 4096, monitored the step changes in voltage. The Nicolet unit is a digital recorder sampling every 2 μ s. The voltage signal was displayed on the screen and stored on a floppy disk for later analysis. With this arrangement a very clear voltage trace was obtained. A typical voltage time record obtained from a BP Rigidex 002-40 pipe at 3 bar pressure is shown in Fig. 5.8. The recorded data can be interpreted with considerable accuracy due to the ability of the Nicolet unit to expand the stored data up to 64x in horizontal and vertical directions. The digital output of the Nicolet was used to find the exact times between each voltage step by moving the cursor across the recorded signals.

5.5. Containment Cage

In ductile crack propagation in steel gas pipeline the main part of the driving force has been thought to come from the pressure acting on the flaps behind the crack tip. Chao (1987) studied the effect of flap constraint on ductile crack propagation, and observed that as the crack entered the mass-augmented pipe section, the increased mass inhibited the crack opening and slowed the crack velocity. When the added mass was too small, the pipe flaps behind the crack tip would eventually begin to open but the crack velocity had slowed down considerably on entering the section. Chao also observed that as the crack left the augmented section and entered the normal section the crack velocity increased until a steady-state was again attained. For wire-wrapped arrester experiments he found that arrest was affected by its specified ultimate strain. Additionally it was found that a loose wire wrapping was the most effective crack arrester while mass augmentation was the least effective. Finally it was concluded that the arrest is affected by geometrically constraining the flaps or increasing their inertia.

If the test pressure is above a certain critical pressure, p_c , the fast crack in a small-scale test has to attain a steady state very soon after initiation. As the pipe specimen used in these tests

is short, generally 7D in length, the crack propagation velocity must be forced to settle to a constant value as soon as it enters the actual test section by artificial means. In the interpretation of other SS test results, such as those from the 'Modified Robertson' test, when the crack stops before it reaches the end of the specimen it is not very certain whether the crack behaviour is to be defined as 'arrest' or 'propagation'. To prevent this kind of ambiguity, a special technique has to be introduced to the test: a 'containment cage' to prevent the flaring of flaps behind the crack tip and to delay the discharge process. The crack velocity measurements have shown that this objective was attained satisfactorily. On the other hand in the case of crack arrest the crack stopped about 1-2 D after the initiation section, indicating clearly an 'arrest' condition. In other words, the boundary between crack arrest and crack propagation is seen at a clear crack tip pressure.

The radial clearance between the pipe and the cage after pressurisation was varied between 1 to 15 mm to see what kind of effect the cage does have. Another objective was to test whether the crack driving force come either from the pressure acting on the flaps, or the strain energy stored in the pipe wall, or a combination of these two.

Our experimental results indicated that the containment cage must allow for the radial expansion of the pipe but must restrict radial flaring of the flaps behind the crack tip. After pressurising the pipe, if there is no clearance between pipe and cage, each ring on the containment cage can behave as a crack arrester, thus causing the propagating crack to stop.

5.6. Test Piece and Procedure

The test piece used is a length of as-extruded pipe, normally of 7D. No special preparation is necessary ^{but} chamfering of the initiation end to facilitate loading onto the core. Although initiation of a fast crack, for most cases, does not require any pre-notch, if that is necessary to initiate RCP, an initial notch having a depth of 1/3 pipe wall thickness along the initiation section can be introduced into the pipe, but there should be no initial notch along any part of the propagation zone.

To carry out a reliable test the following test procedure has to be followed:

- 1) Chamfer one end of the pipe specimen.
- 2) Cool the pipe specimen down to the test temperature and keep it there at least 24 hours before the test.
- 3) Place the specimen in the test rig.
- 4) Locate the containment cage.
- 5) Tighten the end-nut to pre-stress the pressure actuated seals and then start pressurising the pipe.
- 6) Loosen the end-nut slightly in order to reduce the circumferential stress created by the initial nut tightening.
- 7) Press the button to fire the projectile to initiate a crack.
- 8) With the propagation zone unpressurised, establish initiation zone conditions, *i.e.* pressure, impact velocity, and if required pre-notch depth and geometry, to generate a crack penetrating the low-pressure section by a minimum axial length of $D/3$ and a maximum length of $3/2 D$. These calibration experiments can be conducted on short pipe sections.
- 9) Maintaining initiation conditions, vary the propagation section pressure through a series of tests and for each specimen noting the final (arrest) crack length as measured from the beginning of the propagation section.
- 10) Establish the critical pressure, p_C , above which the crack propagates to within a distance D of the end of propagation section. A minimum of three tests have to be performed between pressure of p_C and $1.5p_C$, and a minimum of three between $0.5 p_C$ and p_C .

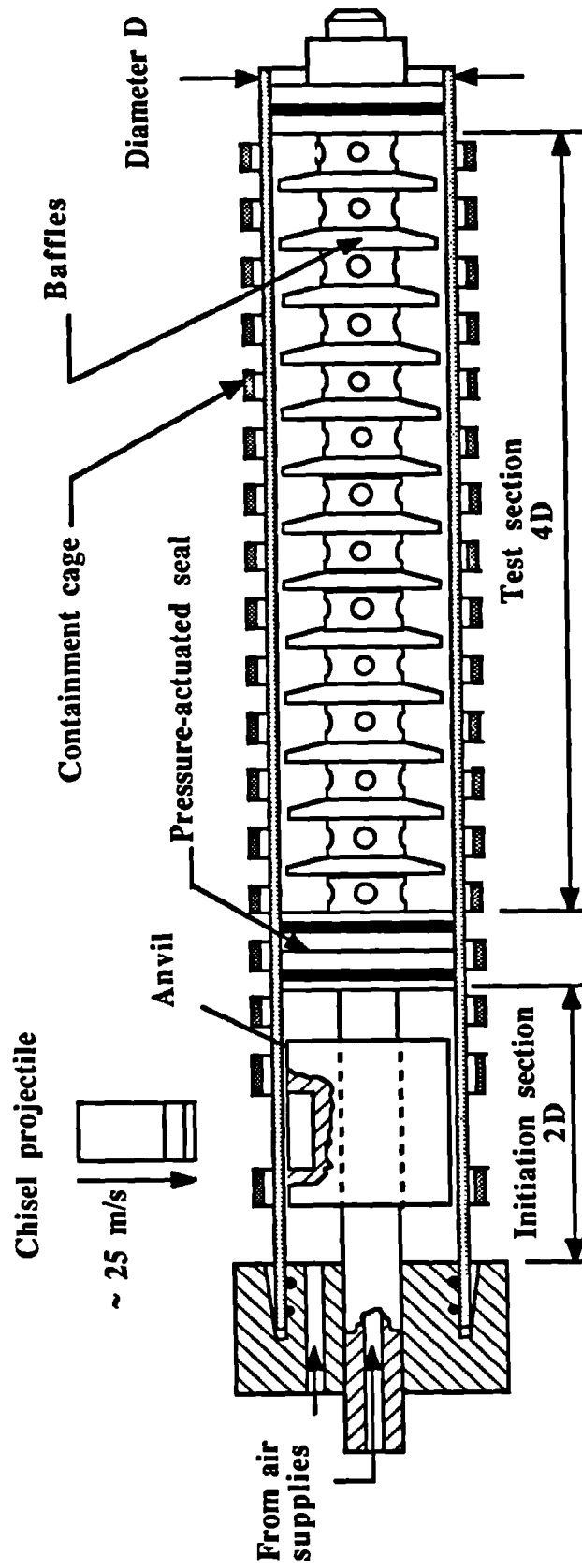


Figure 5.1 Schematic diagram of the Small Scale Steady State (S4) pipe test rig

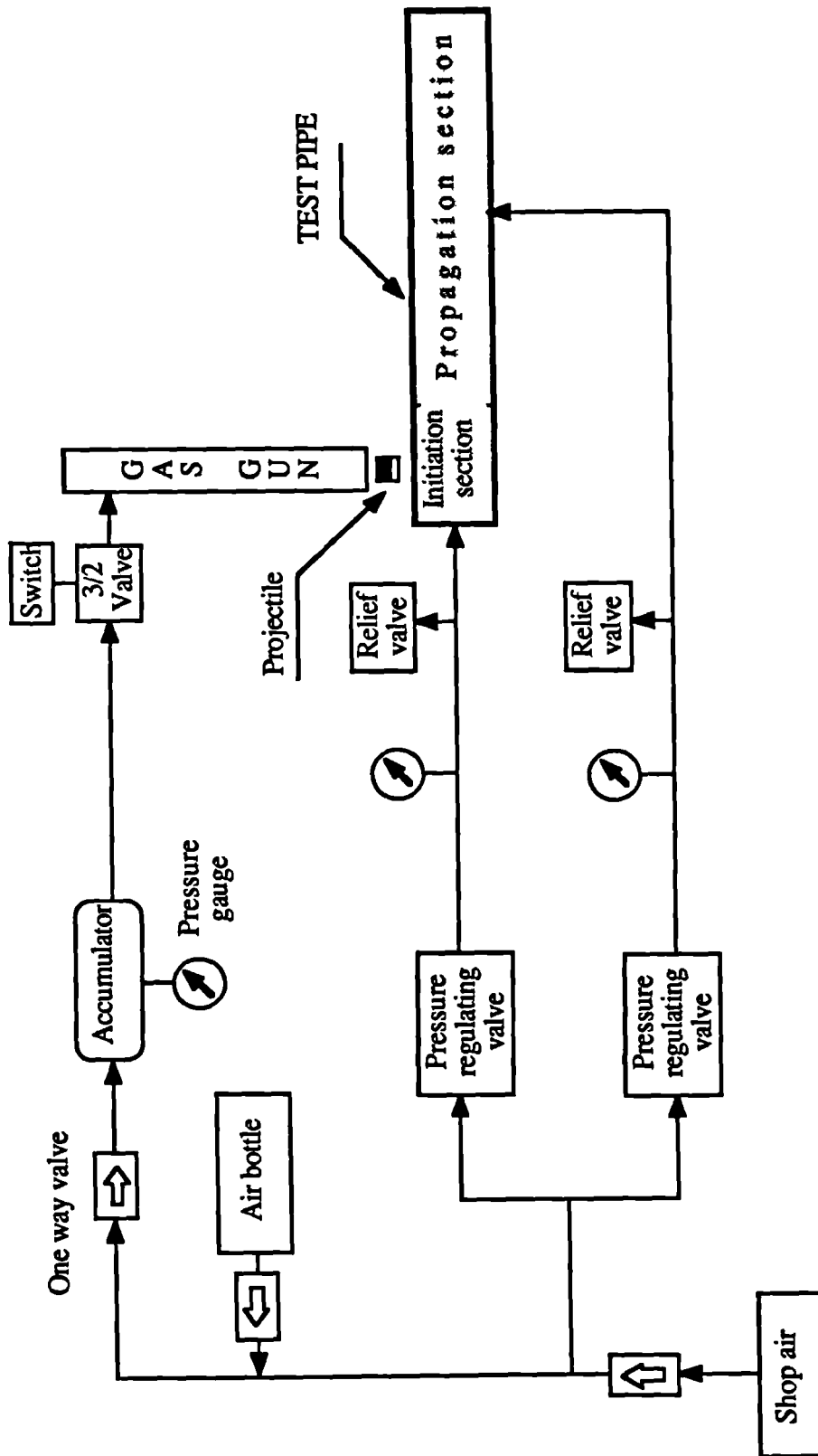


Figure 5.2 Block diagram for the S4 pipe rig pneumatics.

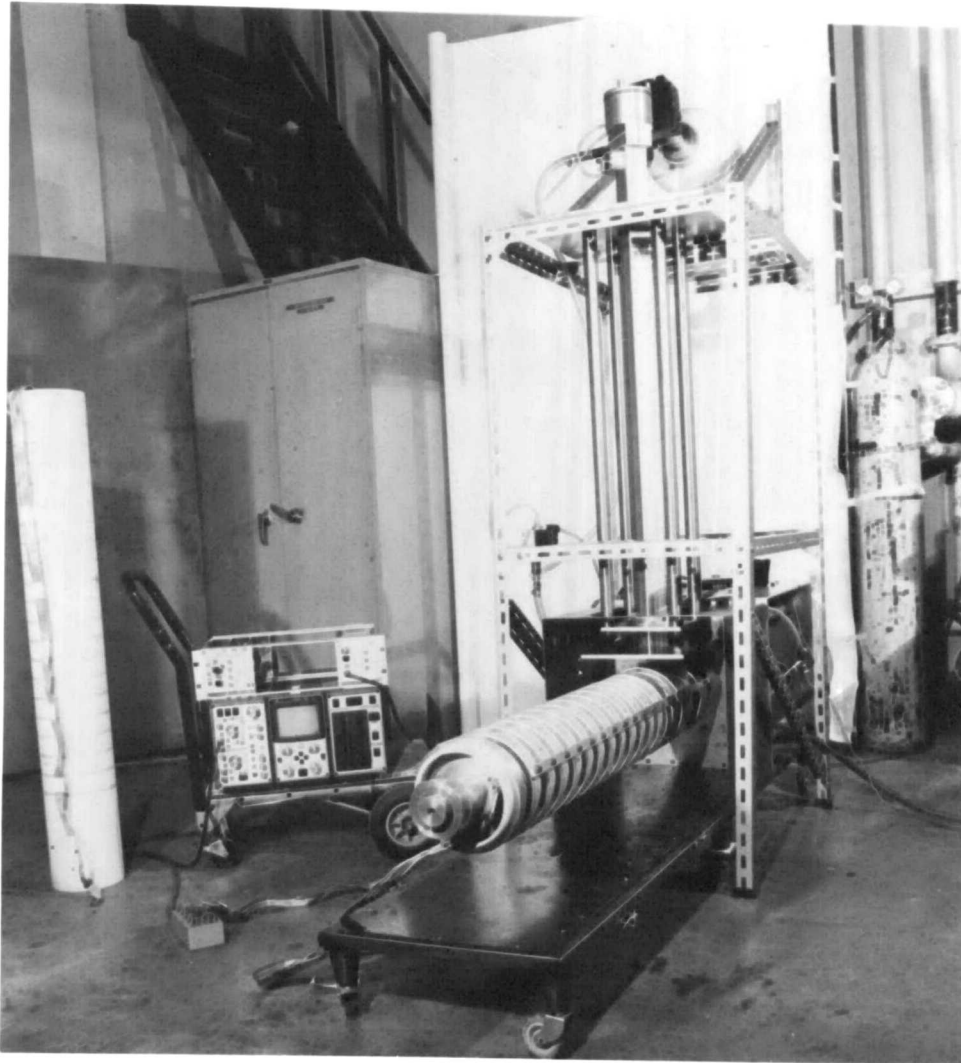


Figure 5.3 S4 test rig for 180 mm SDR 11 and 17.6 pipes.

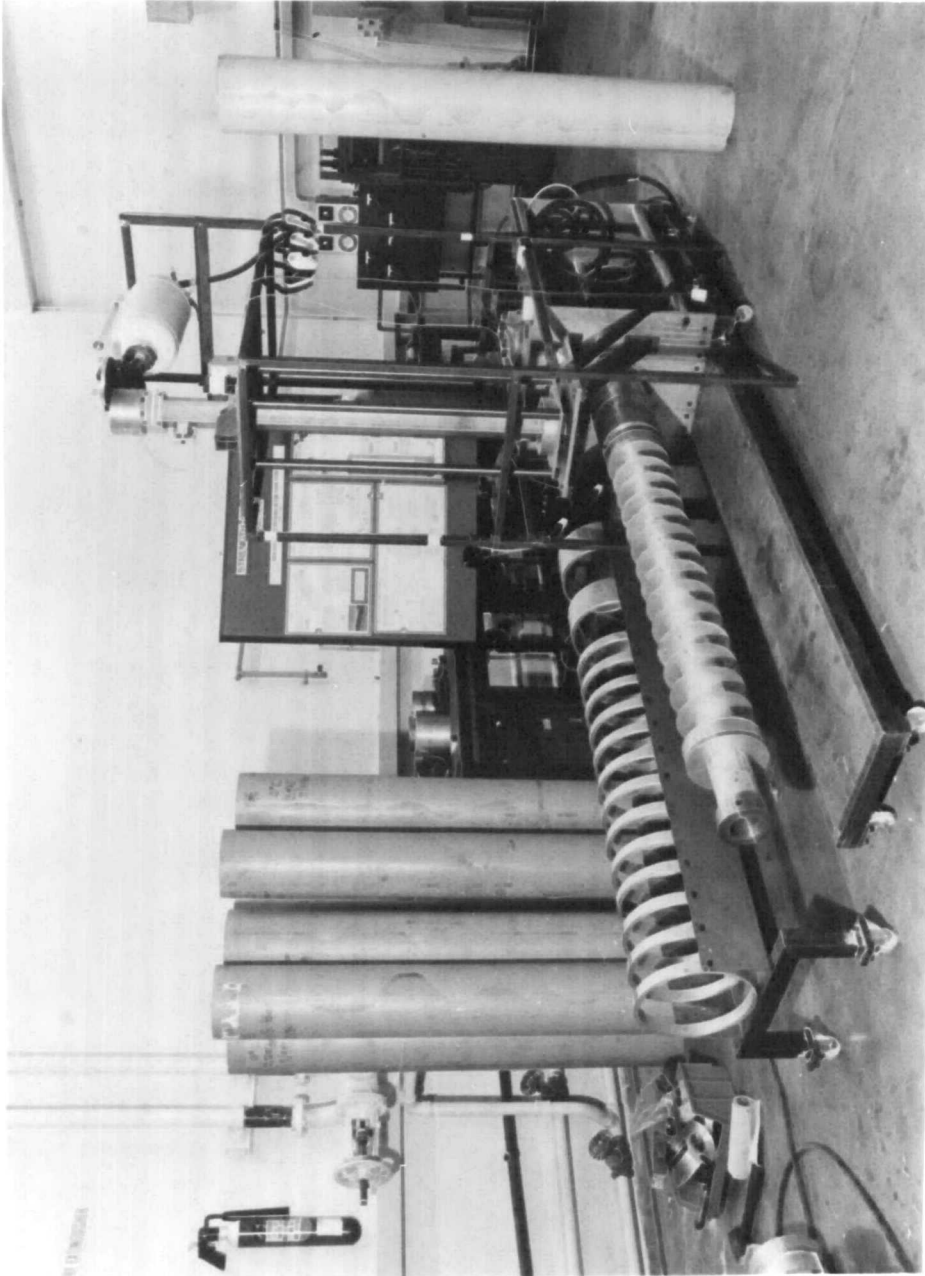


Figure 5.4 S4 test rig for 250 mm SDR 11 and 17.6 pipes.

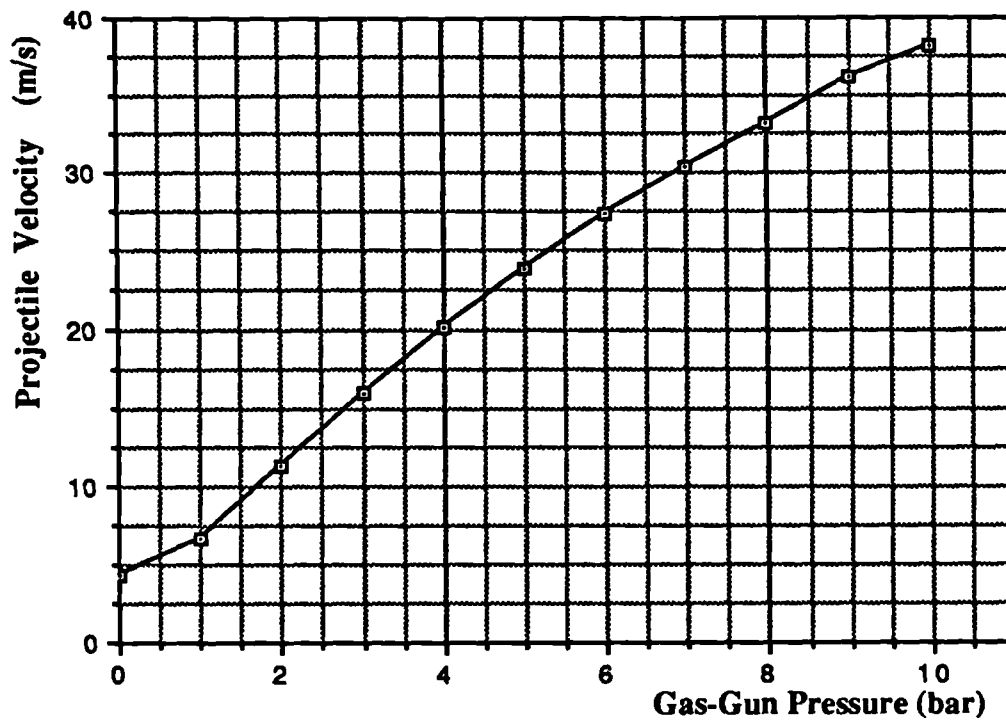


Figure 5.5 Variation of projectile velocity with gas-gun pressure.

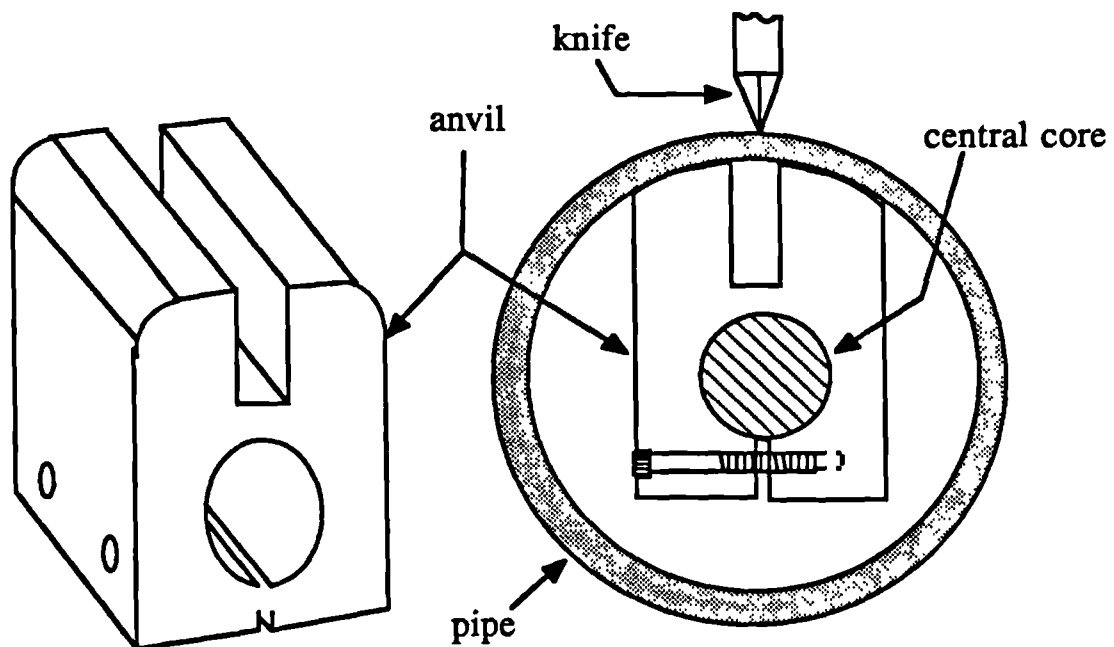


Figure 5.6 Central core used to initiate fast crack.

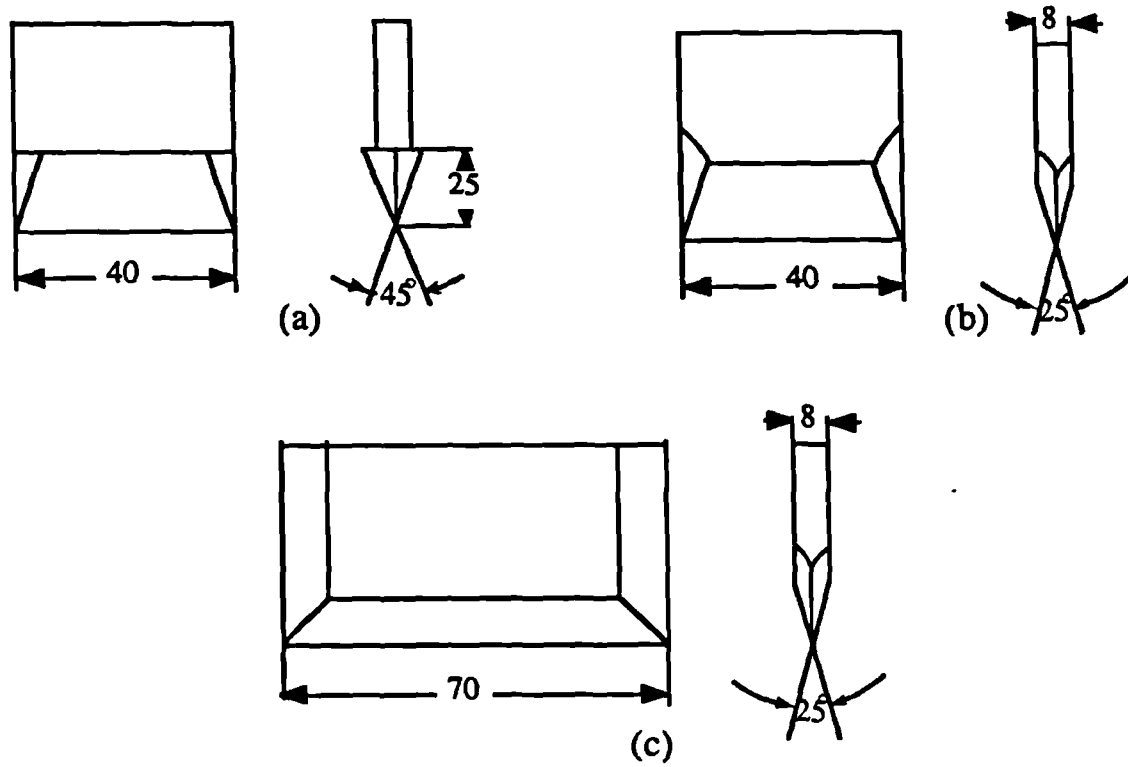


Figure 5.7 Shape and size of blades used in experiments to initiate a fast crack.

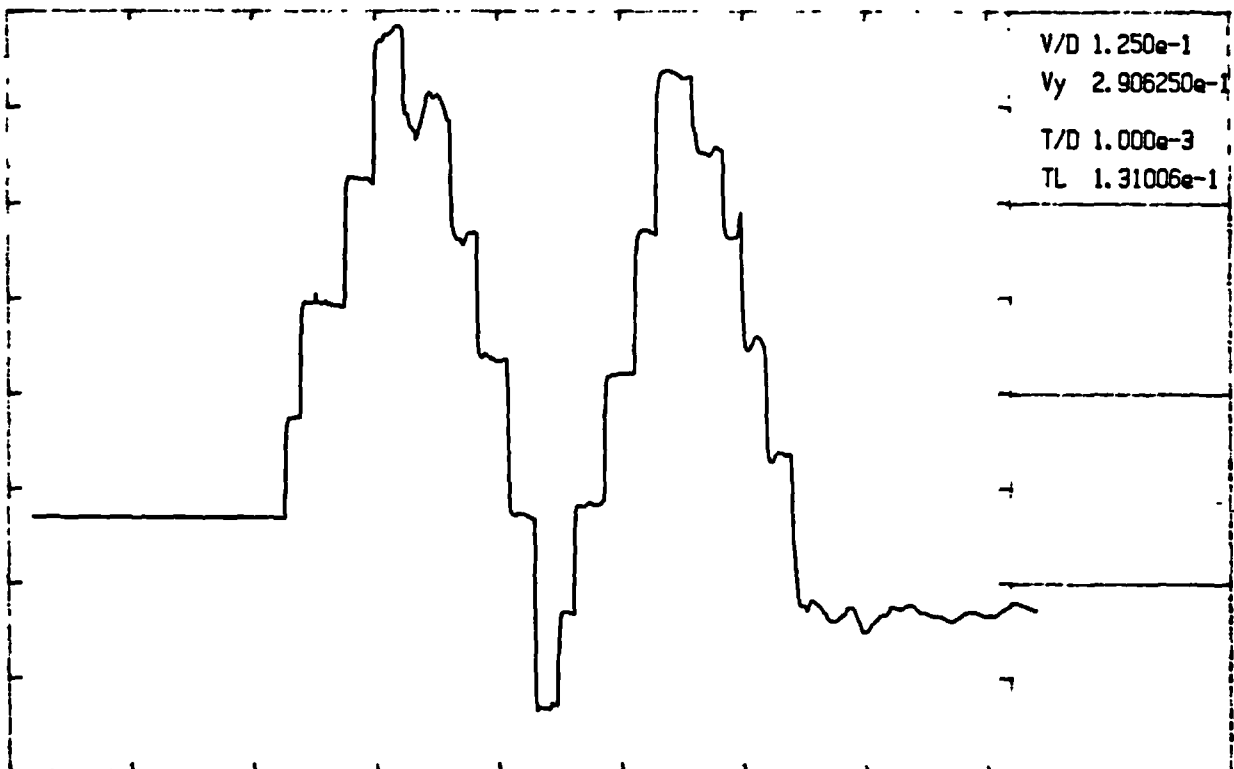


Figure 5.8 Typical voltage time trace recorded for crack velocity measurements.

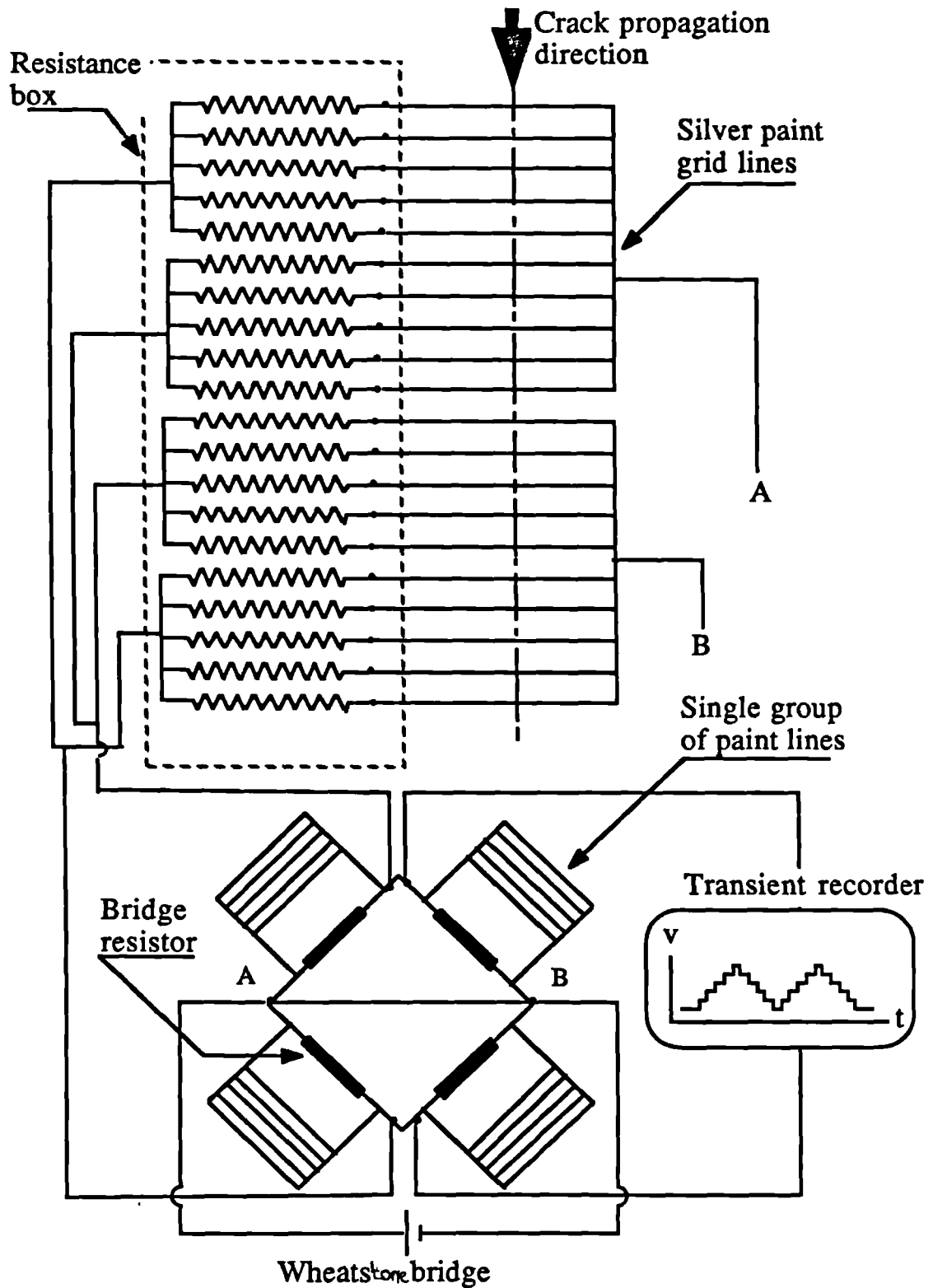


Figure 5.9 Schematic representation of circuit diagram used for crack velocity measurements.

CHAPTER 6

EXPERIMENTAL RESULTS

6.1. Introduction

With the economical and quick small-scale (SS) pipe test technique developed during this current research program, details of which are given in Chapter 4, experiments were carried on various PE gas as well as water pipes. The technique makes it possible to test the pipe under the conditions that are considered: temperature and pressure were varied between -30 °C and 15 °C, and 0 to 25 bar, respectively. During this research program a total number of 50 instrumented pipe experiments for crack velocity measurements and 200 un-instrumented tests for critical pressure definition were conducted on different gas and water PE pipes. Since instrumenting a test pipe and checking all the connections takes up to one day (which compared to the full-scale tests is very reasonable), this number seems to be relatively high. Although there are several experimental test centres all over the world involving RCP in PE pipes this number is, at least so far, the highest that have been reported, and presumably carried out, on both FS and SS test cases to illuminate the nature of the event.

6.2. Crack Initiation

The first observations to be made were the initiation of a fast crack and its final length. To ensure this pre-condition a series of tests was carried out on the material for a range of pipe dimensions and temperatures. A close examination of these calibration tests showed that the resistance of material to crack initiation varies not only from one resin to another but also, for a specific material, with temperature; the higher the test temperature, the higher is the crack initiation resistance. The third parameter affecting the initiation resistance is the thickness of the test pipe. It has been observed that as the thickness increases the initiation of a fast crack

becomes easier, and it requires less attention to make sure that a fast crack has already been initiated before commencing the actual critical pressure tests.

6.2.1. Tests on 180 mm SDR 17.6 and 11 PE Pipe

Before commencing the actual critical pressure definition tests, tests were carried out on 180 mm SDR 17.6 BP 002-40 and Du Pont Aldyl 'A' MDPE gas pipes at different temperatures. It was observed that the initiation of a fast crack was very sensitive to temperature, knife dimensions, initial notch and initiation section pressure. As in other pipe grades the best crack could only be initiated with the blade having 25° tip angle and 50° side angle as shown in Fig. 5.2c. At -15 °C and -10 °C a fast crack could easily be initiated without any initial notch along the initiation section. For these temperatures even 27 ms⁻¹ impact speed (or 6 bar gas-gun pressure) resulted in fast crack initiation.

As the test temperature was shifted to 0 °C and above, a fast crack could not be initiated with the above initiation parameters. For these temperatures an initial notch of 2/3 of the pipe thickness was introduced throughout the initiation section but this also failed to give the required initiation conditions. With this notch configuration the initiation section was pressurised up to 10 bar for each test and a 6 bar gas-gun pressure resulted in a fast crack, but the initiated fast crack stopped as soon as it arrived at the end of the initial notch. To drive this initiated crack further the gas-gun pressure was increased from 6 bar to 10 bar. With this final gas-gun pressure, initial notch and initiation section pressure configurations, a fast crack could be initiated and it propagated at least one diameter beyond from the beginning of the actual test section for temperatures of 0 °C and 10 °C. At 15 °C all these crack initiation strategies failed to produce an initial fast crack. At this temperature, a fast crack could only be initiated when an initial notch was introduced to the pipe along the initiation section and this notch was cooled using liquid nitrogen. Later tests on sharp notched bore pipes indicated this to be the most effective way of introducing a fast crack for any test temperatures up to room temperature.

Similar crack initiation tests were repeated on BP 002-40 and 002-50, 180 SDR 11 yellow gas and blue water pipes, respectively. These tests showed that a fast initial crack could easily be initiated without any initial notch and pressurisation of the initiation section for 0 °C and 10 °C as long as the projectile was fired at 10 bar. This observation implies that as the thickness of the pipe is increased, the resistance of material to crack initiation by this method decreases.

The speed of the projectile is one of the most effective parameters to initiate a fast crack. Higher gas gun pressure gives a higher projectile velocity, which induces a higher impact strain rate around the impact zone causing a faster and longer initial crack. A series of tests was carried out on BP 002-40 180 mm SDR 17.6 PE pipe at -15 °C to find out the effect of IZ pressure upon the final crack length. The results are plotted in Fig. 6.1. The figure implies that the higher the IZ pressure the longer the final crack length at this temperature. However, tests at higher temperatures did not confirm the above result but implied that an unpressurised IZ gives faster and sometimes longer crack lengths. Thus, the magnitude of this pressure has to be calibrated for the test temperature so that the final crack length is within the recommended limit. Longer initial cracks reduce the effective test section length which is not, of course, convenient for such a short length test technique.

6.2.2. Tests on 180 mm SDR 11 TUB 125 Pipe

Similar initiation tests were carried out on 180 mm SDR 11 Solvay TUB 125 gas grade pipe at -15 °C and -10 °C. Depending upon the initiation section pressure the measured total crack length is plotted in Fig. 6.2. In these tests all the available different techniques were tried to initiate a fast and long enough initial crack. These techniques were;

i) Projectile blade angle: A blade having 45° attached to the projectile was used. With this knife dimension a crack could not even be propagated at -15 and -10 °C. The same situation occurred for the other pipe grades and dimensions. With this knife an extensive plastic deformation took place around the impact zone. When the knife angle was changed to

25° a fast crack was successfully initiated. Similar tests were conducted using a knife having 10° tip angle but the results were not satisfactory. Then it was decided that the best angle to use is 25°. The same knife configuration successfully resulted in the required crack initiation conditions at -30 °C, as well.

ii) Introducing an initial notch: In order to introduce a fast crack, a notch having a depth of half the pipe wall thickness was introduced along the initiation section. However, it was discovered that a fast initial crack could not be initiated for any initiation pressure varying from 0 to 20 bar. All of the initiated cracks stopped at the end of the initial notch; also, fracture surface analysis indicated that the initial crack velocities for all cases left remarkably higher ductility on the fracture surfaces. From these observations it can be assumed that under the same circumstances increasing the pipe wall thickness decreases the ductility and presumably increases the crack velocity.

iii) Ideal crack initiation conditions: The calibration tests showed that to initiate a fast crack the best arrangement is to use a pipe without any initial notch. A series of experiments was performed on the pipe to find out the initiation pressure effect on crack initiation behaviour of the material. In these tests the propagation section was unpressurised, and the initiation section was pressurised from 0 up to 20 bar and corresponding crack propagation distances were measured. For -15 °C, not only the longest crack length but also a brittle crack was obtained at zero bar initiation section pressure. Thus, in critical pressure definition tests an unnotched pipe with 0 bar initiation section pressure was used. The variation of initial crack length with initiation section pressure for -15 and -10 °C is shown in Fig. 6.2. As can be seen, at -10 °C all the initiated cracks stopped at the end of initiation section, that is to say, initiation of a fast crack failed at -10 °C.

It is worth pointing out one of the important conclusions of the present investigation: if the initiation of a fast crack fails, or is not fast enough, this does not necessarily mean that RCP could not take place under these circumstances. One particular test result provides a good example for this. Tests on TUB 125 180 SDR 11 pipe at -10 °C showed that although a

crack was initiated under normal test circumstances, its initial velocity was not high enough and it could not propagate in the PZ at least 1D, although the test pressures were increased up to 17 bar, a full length crack propagation could not be obtained. Nevertheless, later tests on specimens in which the initiation section's inner surface was sharply notched indicated RCP at a pressure of 14.5 bar

6.3. Critical Pressure Definition Tests

In modified Robertson (MR) tests, the super-cooled region introduces some uncertainty in interpreting the results. It has been assumed that the cooled zone does not penetrate into the pressurised test section. However, no direct measures have been specified to prevent spilling the liquid nitrogen used to cool the initiation section down, onto the pressurised section. This could induce some scatter into Battelle's SS test results (Krishnaswamy *et al.* 1986). Additionally, the propagation or arrest criteria in MD test is based on the final crack length, but not a clear transition from arrest to total pipe fracture, different initiation conditions could easily result in different final crack arrest length which again could result in misleading interpretation. These sources of scatters were eliminated in the present test technique by not using such a super-cooled section and, not the final crack length but a clear transition pressure was considered as the key parameter in experiments.

In FS tests, as well as the MR test, final crack length and crack velocity are the main parameters used to distinguish between crack arrest and crack propagation. However, due to the decompression behaviour of the pressurised gas and the reflection of the decompression front from the pipe end in the MR test, the maximum attainable final crack length shows considerable scatter and depends very much on the crack velocity, Eq. 2.6.

In the present technique, although the crack arrest length varies with initiation conditions, there is subsequently a very clear transition from arrest to propagation at a well defined pressure value. Therefore, it does not matter whether this transition is after 1D or 2D from the beginning of PZ section: the measured critical pressure will remain the same.

After the calibration tests mentioned above, the actual critical pressure definition tests were carried out on each material under the required test conditions. Following the test procedure described in the previous chapter, tests were carried out on 180 mm SDR 11 TUB 125 PE gas pipe. The variation of crack length with pressure at -15 °C was defined and is shown in Fig. 6.3. From the figure it can be seen that there is a clear transition from crack arrest to crack propagation as the pressure is increased from 9.85 bar to 10 bar. This figure also shows that this transition has taken place after a length of 41 cm from the beginning of the test pipe. For the same material similar critical pressure definition tests were carried out at -30 °C and the results are plotted in Fig. 6.4 showing that there is again a sharp transition pressure from crack arrest to crack propagation at around 3 bar, at a longer transition length, about 70 cm. The similar test results on BP 002-40 180 mm SDR 17.6 PE pipe at -15 °C are shown in Fig. 6.5 indicating a similarly sharp transition pressure of 2.05 bar.

6.4. Crack Velocity Measurements

Despite extensive experimental observation in the past, there are still many curious aspects related to the crack velocity which cannot be explained. For example: how does the crack velocity vary with respect to the pipe dimensions, as well as the test conditions? How does the crack velocity vary when the propagation criteria are not fulfilled? Will it stop abruptly or will it decrease gradually and finally stop? Is there any lower bound value for steady state propagation and, if so, how is it affected by test conditions? What is the maximum attainable crack velocity for a certain pipe material and dimensions? These questions can be extended and it is believed that they must be answered in order to assess the velocity dependent analysis.

Two typical crack velocity histories derived from the timing wire traces for BP 002-40 180 mm SDR 17.6 PE pipe at -15 °C and 4.0 bar and 2.0 bar are shown in Figs. 6.6 and 6.7, respectively. For a particular material, dimensions and temperature, the initial crack velocity was found to depend very much upon the speed of projectile, and the IZ pressure. As the speed of projectile and/or the IZ pressure were increased the initial crack velocity increased.

Beside this, as the test temperature was decreased and/or pipe wall thickness was increased the value of initial crack velocity increased. Depending upon these parameters in all the tests the value of recorded initial crack velocity varied between 700 ms^{-1} and 250 ms^{-1} . The experimental results, reported elsewhere (Yayla & Leever, 1989), have shown that whatever the value of initial crack velocity was, it decreased continuously as the crack advanced. On crossing the transition seal, the crack velocity stabilised to a particular average value along the test section for $p > p_c$, or rapidly fell to zero as shown schematically in Fig. 6.8. It is these measured steady-state crack velocities for $p > p_c$, on such short pipe lengths, which lead us to name the newly developed test technique as the Small-Scale-Steady-State (or S4) technique and the critical pressure results obtained from the S4 test as p_{cS4} .

For various PZ pressures typical crack velocity histories at -15°C for Du Pont Aldyl 'A' and BP 002-40 180 mm SDR 17.6 pipes are shown in Figs. 6.9 and 6.10, respectively. It should be emphasised that the traces shown are absolutely typical, and that the data have already been differentiated, without smoothing, from the original crack length versus time trace. Although there is scatter in the measured axial crack velocity, this velocity variation still appears to be steadier than in FS tests, confirming the localising influence of the decompression baffles. The increase in velocity at the end of the pipe shown in Fig. 6.9 and Fig. 6.10 is probably due to the additional hoop stresses introduced into the pipe by the end seal. Another observation worth mentioning is that if the crack propagates along a straight path or with relatively little lateral oscillation, the axial crack velocity takes a more or less constant value along the PZ. There is also a tendency that as the test pressure increases the amplitude of crack path oscillations decreases. From these two observations as can be seen from Figs. 6.9 and 6.10, for lower test pressures there are relatively large variations in crack velocity. As the test pressure increases the crack velocity takes a comparatively constant value along the PZ. One of the main reasons for this axial crack velocity dependence on the shape of crack path is that the velocity of the crack tip varies along the oscillating crack path. This topic is discussed in more detail in the following chapter.

From the extensive crack velocity recordings obtained during this research program, some conclusions can be summarised as follows:

1) After initiating a fast initial crack, the crack reaches a steady-state velocity if the test pressure for these test conditions is above a well defined critical value. With this test technique, in all crack velocity measurements it was observed that this steady state condition is attained after a very short length of crack propagation, generally less than one diameter from the beginning of the test section. By contrast, if the test pressure is below a certain value, crack velocity continuously decreases and stops after propagating only a short length. This final crack arrest length is not a definite and constant value for every material and test parameter but, depending upon the test variables, varies between 1D and 2.5D from the beginning of the test section. In none of the experiments performed did the final crack length at arrest exceed 2.5D. This is a very desirable outcome of the technique in that for all experiments it was very easy to distinguish 'propagation' from 'arrest'.

2) The crack initiation conditions do not have any significant effect upon the average crack velocity along the test section.

6.5. Temperature Effects

The investigation of temperature effects on RCP performance of PE pipes was of the greatest importance in this research project. Thus, with the test method developed, several series of experiments were performed on different PE pipes to determine p_{CS4} at different temperatures. The most significant outcome of these experiments was that RCP phenomenon of PE pipes under investigation is very sensitive to the test temperature. For BP 002-40 180 mm SDR 17.6 pipe, the variation of critical pressure with temperature is illustrated in Fig. 6.11. As can be seen from this figure, there is a slight increase in critical pressure (from 2.05 to 2.1 bar) as the test temperature is shifted from -15 °C to 0 °C but a further increase to 10 °C raises the critical pressure to 4.0 bar. This is in good agreement with the BG

observation on FS tests that the increase of temperature from 3 °C to 12 °C has raised the critical pressure from 7 bar to 14 bar.

Similar critical pressure definition tests were carried out at 15 °C. For this temperature the initiation of a fast crack failed for all the conventional initiation parameters. Then an initial notch was introduced into the pipe along the initiation section cooled down by filling it with liquid nitrogen. After this cooling process tests were performed on the same material. However, although the pressure was increased up to 15 bar, crack propagation failed after advancing about 5 to 8 cm into the unnotched section. Examination of the fracture surfaces revealed that for this temperature the crack velocity is too low to sustain propagation. As the pressure is increased, due to the very low crack velocity, crack tip blunting occurs, consequently the material's resistance to crack propagation increases considerably and it becomes almost impossible to run the crack into it. To extend these experiments, the temperature was shifted to room temperature (≈ 22 °C). The initiated cracks arrested at all pressures up to 15 bar after propagating only for about 2 to 5 cm beyond the initial notch.

In Fig. 6.12 the variation of critical pressure with temperature for TUB 125 between -30 and 0 °C is shown. Unlike that of BP 002-40, the p_{CS4} value continuously increased within this temperature range, but there is a similarity in that above some temperature (0 °C), RCP was not observed for any pressure value up to 25 bar.

Besides the variation of the critical pressure with temperature, another important effect of temperature on RCP behaviour is the variation of crack velocity with temperature at constant pressure. To investigate this important effect experiments were carried on 180 mm SDR 17.6 BP 002-40 pipe at a constant pressure of 2.6 bar, but at different temperatures. The average crack velocity is plotted against temperature in Fig. 6.13 and reported elsewhere (Yayla & Leever, 1990). The figure shows that crack velocity decreases slightly with temperature as the temperature is increased from -20 °C to -5 °C but small differences in temperature around 0 °C have a drastic effect on steady state crack velocity. The relatively low steady state crack velocity along the test section which is shown in Fig. 6.14 is very

important, in that it will allow greater decompression in the FS test, to $0.33 p_I$. The dips in the crack velocity history match crack arrest attempt marks on the fracture surfaces, and indicate a strong interaction between the crack velocity and the driving force coming from strain energy stored in the pipe and from the pressure acting on the flaps behind the crack tip.

Similar tests have been carried out on 180 mm SDR 11 TUB 125 gas pipe to evaluate the critical pressure at various temperatures. As shown in Fig. 6.3 and Fig. 6.4, although the critical pressure at $-30\text{ }^{\circ}\text{C}$ is about 3 bar, as the temperature is increased to $-15\text{ }^{\circ}\text{C}$ the p_{CS4} jumps to 10 bar.

These tests imply that, firstly, temperature is one of the main parameters determining the RCP performance of PE pipes, and secondly, for each PE pipe grade there is a certain transition temperature above which p_{CS4} increases in a very sharp manner. Furthermore, if the test temperature is well above this transition temperature, RCP performance becomes so high that a fast crack cannot be initiated.

6.6. Thickness (SDR) Effects

One of the basic principle of Linear Elastic Fracture Mechanics (LEFM) is that, for a given material, a critical value of the stress intensity factor, termed 'fracture toughness', controls the fracture process. Fracture toughness values are usually dependent on the thickness of the specimen furnishing them. It has long been known that the thickness of a test sample has a significant effect on its fracture toughness. ASTM standards acknowledge a certain minimum thickness for a reliable fracture toughness test and this value is related to the size of the near-tip plastic zone. This thickness requirement is given by

$$B \geq 2.5 \left(\frac{K_{IC}}{\sigma_Y} \right)^2 \quad (6.1)$$

where B is the required minimum specimen thickness and σ_Y is the yield stress of the material. This requirement suggests that the transition from plane stress to plane strain is about 50 mm for PE. However, for engineering plastics, unlike metallic materials, it is

almost impossible to generalise the above requirements because of their viscoelastic nature. This relation clearly implies that in order to increase the material's resistance to any form of brittle crack propagation and to induce truly ductile failure, there are three main avenues open (Marshall *et al.*, 1988):

- i) Reduce the thickness of the material.
- ii) Increase the crack resistance of material to a level where yielding intervenes before crack propagation.
- iii) Reduce the yield strength of material to encourage yield to occur.

These three parameters are the most effective quantities to increase RCP resistance of PE pipes. However, another problem related with the design of PE pipelines is 'slow crack growth', SCG. To improve the resistance of the material to SCG, some of the above three recommendations should be reversed. To conclude, at the design stage of PE pipe systems these two different and contradictory failure modes have to be taken into account separately but an optimum condition should be found to avoid both.

During RCP in carbon steel, Rosakis (1989) employed a high speed infrared sensor to monitor the temperature rise on both sides of the propagating crack. With this technique he observed a maximum temperature increase of 500 °C very close to the crack tip. Repeating these measurements on side grooved samples he observed only 200 °C temperature rise, demonstrating the effect of shear lips on the fracture event. From this study he reported that more than 70% of the fracture energy was dissipated in the lips during crack propagation.

To investigate the effect of pipe thickness, *i.e.* SDR, on crack velocity history a special test technique was developed and used. In this technique, along one pipe, five different sections having different thicknesses were machined on a vertical lathe. The pipe was located on the machine and calibrated to eliminate any radial deviation. The other end of the pipe was aligned with a live centre in the tailstock. It must be mentioned that it is almost impossible to eliminate all the radial deviation, because of the lack of roundness of the pipe due to the

residual stress effect and other manufacturing processes. After minimizing this irregularity and locating the pipe to the machine correctly by inserting two mandrels into both ends of the pipe to provide rigidity, each section of the pipe was turned down at a speed of 70 rpm to the required thickness. No more than 0.1 mm material was removed from the surface for a given step and a very sharp lathe blade was used. The final shape and dimensions of the pipe are schematically shown in Fig. 6.15.

The tests were carried out on BP 002-40 180 SDR 11 PE gas pipe at 0 °C. After instrumenting the pipe with timing wires to measure the velocity history, and cooling it down to 0 °C, tests were performed on the pipe at 3.5 bar test pressure, which is about 55% higher than the critical pressure for the original pipe (2.25 bar). The recorded crack velocity profile is shown in Fig. 6.16, showing that as the crack enters a thinner section, its velocity reduces abruptly to a new steady state value. The crack arrested as it entered to the thinnest area of the wall, suggesting that there is a minimum crack velocity below which the propagation of the crack cannot be sustained. The variation of the crack velocity with pipe wall thickness is plotted in Fig. 6.17. This variation is of great importance in RCP and clearly implies that the crack velocity linearly decreases with decreasing pipe wall thickness. However, the minimum sustainable crack velocity limit, which is about 70 ms^{-1} for this material under test conditions, defines the minimum pipe wall thickness below which a propagating crack cannot be sustained at this pressure. It is interesting to note that this minimum crack velocity limit from these tests agrees well with that of measured for BP 002-40 180 SDR 17.6 pipe at the same temperature (see Section 6.5).

The effect of SDR on both crack velocity history and critical pressure value might be explained by two separate factors. The first is the increasing magnitude of the pipe wall thinning process. As the thickness is decreased plane stress conditions dominate fracture resulting in more energy dissipation by highly dissipative viscoelastic stretching in front of the crack tip, from which the energy is only recovered much later. Shannon & Wells (1974) acknowledged that in steel pipelines, this deformation is plastic rather than elastic and extends about one pipe radius on each side of the crack path. This stretching is predominantly in the

axial direction and axial mid-surface strains of up to 15% have been observed by the same authors. Due to the viscoelastic properties of PE pipes such a highly plastically necked zone is very localised, but as the thickness decreases the magnitude of the thinning increases gradually (it cannot be as high as those observed in ductile RCP in steel pipelines). The second explanation of this crack velocity variation with decreasing pipe wall thickness could be that subsequent examination of the fracture surface revealed an obvious increase in ductility and a growth in the width of the tear lips, which increases the crack propagation resistance of the material, as shown in Figs. 6.18 and 6.19.

In order to check the accuracy and validity of the tapered pipe results, the configuration of step-tapering was reversed, as schematically shown in Fig. 6.20. A series of tests was carried out on these step-tapered BP 002-40 SDR 11 pipes at 0 °C. The test pressure was varied between 2 bar and 13 bar. Test results indicated that as the initiated fast crack traversed from the thickest to the thinnest section (4.5 mm) it suddenly stopped. Increasing the test pressure increased the final crack length from 1.5 to 3 cm in the thinnest section. The fracture surface of this test is shown in Fig. 6.21, indicating very high ductility and some thinning around the crack arrest point.

The influence of pipe wall thickness at constant diameter was assessed by testing a series of 180 mm SDR 11 instead of SDR 17.6, BP 002-40, and BP 002-50 PE gas and water pipe. The results are shown in Fig. 6.22 and Fig. 6.23, respectively. The figures show a critical pressure of 2.25 bar for both of these pipes at 0 °C, compared to 2.1 bar for BP 002-40 180 SDR 17.6 pipe. As a result, a 70% increase in thickness (10 mm to 17.1 mm) has increased p_{CS4} only by about 7%. This is a rather surprising result, which, if backed up by FS testing, would be of immense commercial significance.

To explore the thickness effect on p_{CS4} results, a series of 180 mm SDR 11 BP 002-40 pipes was turned down to specific thicknesses along the test section and tested. During the turning down process maximum care was taken not to affect the microstructure or disturb the residual stress profile. Despite these efforts, it has to be noted that the use of machining to reduce the

pipe wall to the required thicknesses is open to question, on the grounds that it might have some influence on the results.

The results of these tests are shown in Fig. 6.24 for a range of thickness values. As seen, the p_{CS4} result increases almost linearly with thickness. This dependence of p_{CS4} value upon the thickness agrees with the (SDR^{-1}) factor promised by the strain energy model. However, there is a well defined critical thickness below which a RCP is not sustainable even for a pressure well above the critical pressure expected from the extrapolation of the results for thicker pipes.

One important observation has to be pointed out: the critical thickness found from the step-tapered pipe experiments (Fig. 6.16) correlates with the results shown in Fig. 6.24. Consequently, if the step-tapered pipe is used to determine the critical thickness only one test is required, compared with a series of experiments required with constant thickness pipes.

From the post-mortem examination of the fracture surfaces and the results of critical pressure variation with thickness, the mechanism underlying and controlling the thickness effect could be explained as follows. As depicted schematically in Fig. 6.25, the trend of thickness effect upon p_{CS4} could be divided into two regimes: deformation controlled and stress controlled. The former dominates RCP if the thickness of the pipe is below some minimum *critical thickness*. The experimental observations suggested that the crack cannot propagate below some minimum velocity and if that is the case, increasing the test pressure does not increase the crack velocity but intensifies plastic flow in front of the crack tip, reducing the rate of flow of energy to the crack tip and, accordingly, raising the resistance of the material to crack propagation.

For the fracture process the stress controlled regime, the ductility on the fracture surfaces diminishes as the thickness is increased. Although increasing the thickness amplifies the fracture energy per unit pipe length as the thickness increases the viscoelastic nature of the material is suppressed and it tends to behave more like an elastic material, thus fracture process becomes more close to the plain-strain mode.

In addition to the tests on MDPE pipe, experiments were also conducted on 180 mm pipe having different SDR at -15 °C. The test results indicated p_{CS4} for the SDR 11 pipe to be 10 bar, but similar experiments on SDR 17.6 HDPE pipe failed to sustain RCP at pressures up to 16.3 bar. Moreover, a 180 mm SDR 11 TUB 125 pipe was step-tapered as in Fig. 6.15, and tested at -15 °C at a pressure of 12.5 bar (about 25% higher than the critical pressure for the original pipe). The initiated crack propagated along the original thickness of 17.1 mm and entered the thinner section of 16.75 mm in thickness. Finally, after entering the next section which was 15.35 mm thick, it stopped. Similar to the trend seen in the step-tapered pipe experiments on BP 002-40, tests on TUB 125 180 SDR 11 pipe indicated a striking ductility increase on the fracture surfaces as the crack entered the thinner section.

It could be concluded that the critical thickness, which increases with temperature and also varies from material to material, may serve as a useful additional index of pipe RCP performance.

6.7. Effects of Pressure on Crack Velocity

One of the parameters determining the value of decompressed pressure at the crack tip in FS tests is the crack velocity (Eq. 2.6). Nevertheless, it has long been known that the propagating crack velocity does not have a unique value, but is affected by many parameters. One of these is the initial line pressure, but until this wide range of investigations was carried out there were no available data showing the variation of crack velocity with local pressure. Investigations of this variation by other available SS test techniques seem less informative and more difficult to interpret, in that due to decompression and restricted test length the crack tip pressure continuously decreases as the crack proceeds. With the current technique, even at quite low propagation velocities a steady-state condition was achieved within the PZ, *i.e.* arrest seemed not to be due to any pressure drop ahead of crack tip, and it was possible to explore the variation of crack velocity with PZ section pressure.

A series of experiments on BP 002-40 180 mm SDR 17.6 gas pipe was carried out at -15 °C. The crack velocity was taken as the average of the measured values along PZ. The variation of this averaged crack velocity with PZ pressure is plotted in Fig. 6.26. This figure shows two interesting velocity limits. The first one is that the maximum attainable velocity, asymptotically approached, appears to be in excellent agreement with the limit predicted by Kanninen (1970):

$$v_c = \frac{3}{4} c_l \sqrt{\frac{2}{SDR}} \quad (6.2)$$

which, using the value of 2.0 GPa for the material's modulus and 940 kgm⁻³ for the density to evaluate the pipe material's longitudinal wave velocity,

$$c_l = \sqrt{\frac{E}{\rho}} \quad (6.3)$$

becomes about 375 ms⁻¹. The moduli and density of these materials were measured as part of the materials testing program, to be reported elsewhere, Wheel & Leever (1990). This maximum crack velocity also implies that if RCP occurs in a natural gas pipe, even at very high pressure level, the crack tip pressure will always be smaller than the line pressure (the decompression speed of natural gas is about 420 ms⁻¹).

The second interesting observation is that there appears to be a minimum sustainable velocity, about 140 ms⁻¹ at -15 °C, below which the crack decelerates and arrests after little more than one diameter length of axial growth within PZ.

6.8. Influence of the Gas Volume

Since the S4 technique suppresses the decompression by its design features (*i.e.* baffles and cage), it seems to conform closely to the assumption, made by Irwin-Corten analysis for critical pressure, that strain energy alone provides the driving force and the whole of the energy stored in the pipe goes to the fracture process. To test the validity of the theory the

inside volume of pipe PZ was reduced by up to 88% by inserting annular wooden packing discs, as shown in Fig. 6.27, and S4 tests were carried out at this reduced volume on BP 002 40 180 SDR 17.6 pipe at 0 °C. The results showed that p_{cS4} increased from 2.1 to 4.75 bar, although, as Fig. 6.28 indicates, the transition from arrest to propagation took place in a wider pressure range and became more poorly defined. Simple extrapolation of the case for which none of the pipe volume is occupied by air predicts a p_{cS4} of 5.5 bar. Whilst these experiments indicate the pressure acting on the flaps behind the crack tip makes a significant contribution to the crack driving force: the strain energy contribution is only about 15%.

BG tests (Greig, 1988) on 250 mm MDPE pipes pressurised by water, or water and air, show that p_c is much higher than for air alone. These tests indicated that RCP was not possible at a pressure up to 20 bar when the pipe is completely filled with water. Tests with a 10% air above the water indicated p_c of 11.5 bar. Thus, having 10% of air almost doubled the p_c which agrees well with the afore mentioned p_{cS4} results.

One of the striking features of the fractured pipes, tested with reduced volume, was that crack path oscillation reduced significantly. The crack usually propagated in a straight manner, emphasising the contribution of pressure acting on the pipe flaps behind the crack tip to crack oscillation.

Despite these correlations, there is an obvious analogy here to the replacement of air by water in FS tests, although the air in a modified S4 test will occupy an annular volume whilst in FS tests it will rise to the top.

6.9. Processing Effects

It is worth pointing out that a PE pipe is a structure, rather than just an ordinary material. Thus during processing and manufacturing (for PE pipes, extrusion) the material could undergo a number of different structural changes. The same grade of PE extruded into pipe by different types of extruder could change in RCP performance.

Tests on similar TUB 125 180 mm SDR 11 pipe at -15 °C of 'regraind' and 'virgin' material, extruded by the same manufacturer, indicated p_{CS4} as 10 bar and 9.25 bar, respectively. Additionally, tests on TUB 124 180 mm SDR 11 pipes at -15 °C, extruded by different manufacturer, indicated p_{CS4} values as 7.5 bar. This dependence of p_{CS4} upon pipe processing indicates some variation in performance, but it is not yet known whether this is because of different residual stress patterns in the pipes, or the percentage of crystallinity of the material, or pigments.

6.9. Influence of the Containment Cage

One of the main features of the technique developed is the use of a 'containment cage'. It is worth mentioning how this idea came about and was used in the final exploratory experiments.

The usage of 'baffles' is essential for a reliable test to suppress the decompression of air while the crack is propagating. To arrive at this target successfully, the gap between the inside diameter of the pipe and outside diameter of baffles has to be as small as possible, at least not larger than 5% of the pipe inside diameter. Our preliminary experiments without the containment cage showed that if this condition is not satisfied the internal pressure decays, especially for relatively slow crack speeds, during crack propagation. On the contrary, if the gap was within the allowed limits, another problem was faced: the arrest of the propagating crack by the baffles. Flaring of the flaps behind the crack tip by the pressure acting upon them causes axial stretching at and in front of the crack tip, resulting in ovalisation. When the recommended baffle size was used the baffle in front of the crack tip prevented this ovalisation and introduced severe damage to the pipe bore and, presumably, caused the crack to stop. Consequently, the flaring of the flaps behind the crack tip, somehow had to be suppressed or restricted. Then, it was decided that one of the most practical way to do so is to use a 'containment cage'. The preliminary experiments have revealed that the ovalisation was minimised by the usage of the cage.

Another reason for using this containment cage is that as the pipe sample used in the tests is short, the initiated crack has to be encouraged to attain a steady state condition very soon after initiation. The crack velocity histories as well as FE results demonstrated that this purpose was achieved when the cage was used in the experiments and numerical analysis.

Two cages having different sizes were used in the experiments. By using these two different cages, tests were carried out on 180 SDR 17.6 PE pipes. With the former one, a radial clearance of 1 to 5 mm remained between the pipe and the cage. When the gap was decreased from 5 to 1 mm both the amplitude and wavelength of sinusoidal crack path decreased. When the clearance was 1 mm the crack path was almost straight. It was determined by testing at 13 bar and 10 °C that if the pipe expanded such that there was contact with the cage, each ring acted as a crack arrester, but even a very small (≈ 1 mm) clearance between the pipe and cage was sufficient to allow the initiated crack to propagate throughout the PZ at just 4.0 bar.

It was necessary to establish that the exact clearance between the pipe surface and the containment cage, a parameter which is both artificial and poorly controlled, did not exert undue influence on the critical pressure results. Repeating the tests with an increased nominal clearance of 15 mm at 0 °C increased the critical pressure only from 2.1 to 2.2 bar. This slight dependence of critical pressure on the clearance has not yet been understood. The FE code WHAMSE results of Chapter 8 show that the greater the clearance the higher the crack driving force at constant velocity of 145 ms^{-1} , in other words, the higher the clearance the lower the critical pressure. (The computational results from this FE code are discussed in more detail in Chapter 8).

One possible explanation for this increasing critical pressure could be due to the fact that in experiments as the clearance is increased there is more space available for the flaps to flare which causes the pressure behind the crack tip to drop and consequently, reduces the magnitude of the crack driving force, which increases the critical pressure. Since, at least for the time being, there is not any available information about the magnitude of this pressure

decay, in the FE code the pressure profile behind the crack tip is assumed constant at the line pressure.

The second reason for the increasing critical pressure with higher clearance could be as follows. As mentioned above, higher clearance allows more radial flaring which induces not only higher axial stress to the crack tip region, causing axial stretching, but also higher circumferential stress at the crack tip zone. These higher stresses are the main cause of some deformation such as ovalisation and elastic thinning at the crack tip. It is believed that these two deformation processes could be very energy dissipative and that each of these two energy sinks reduce the energy coming to the crack tip. Thus, if the conditions for these energy sinks are arranged by any means, higher pressure is required for the crack to propagate further.

To sum up the effect of clearance on the critical pressure value: increasing the gap tends to give a slightly higher critical pressure. At this stage the cage was removed completely and the critical pressure proved to be unchanged. The apparent conflict between this result and the earlier failure to achieve any crack propagation at much higher pressure is entirely consistent with observations that the 'Modified Robertson' pipe test often fails to sustain propagation at pressures which exceed the critical pressure by a small amount; this is presumably because the excess initial pressure drives faster decompression by a transient burst mode. The last thing worth mentioning is that use of the cage makes the tests safer and, in the case of bifurcation or fragmentation, it restricts the shattering of each fragment.

6.10. Slitting Effect

When the results obtained from normal S4 tests on pipes of MDPE and HDPE indicated significant ductility on the fracture surface (particularly at the inner surface) it was understood that the source of high p_{CS4} , thus high toughness, lies in the ability of the tear lips to absorb energy. To eliminate formation of these tear lips, the test pipe was first sharply notched on the outer surface along the top of the pipe (the expected crack propagation path). The notch was introduced by a sharp razor blade and the slit depth was around 1.5 - 2.0 mm. Tests on

both MDPE and HDPE pipes showed that crack did not follow the slit and thus the p_{cS4} results remained the same as unnotched pipe results. This result was expected since post-mortem investigation of the fracture surfaces had clearly indicated that the tear lips at the outer surface could hardly be seen and were negligible compared with the tear lips seen on the inner side. In the second series of experiments, the pipes were slit at their inner surfaces along the prospective crack path. Tests on these slit BP 002-40 180 SDR 11 pipes were carried out at $-15\text{ }^{\circ}\text{C}$ at pressure values of 0.5 up to 2 bar. In all the tests the initiated fast crack did not follow the slit but either curved away from the slit or branched and finally stopped. Thus, having the slit on this pipe did not pose any influence on RCP results at this temperature. On the other hand, tests on the inner surface slit TUB 125 180 SDR 11 pipes at $-15\text{ }^{\circ}\text{C}$ revealed that the initiated fast crack followed the slit and reduced the critical pressure (which had been 10 bar for the unslit pipe) to 0.75 bar.

A series of experiments was carried out at $0\text{ }^{\circ}\text{C}$ on both PE grades. The test results indicated similar trend for BP 002-40 pipe, that is, the crack did not follow the internal slit but deviated away from it, and gave similar results as in unslit pipe. On the contrary, for TUB 125 pipe the crack followed the slit and gave a p_{cS4} of 0.85 bar.

In the third series of experiments, the test temperature was shifted to room temperature and similar tests were carried out on the same grades. This time the initiated crack followed the internal slit for both grades. The test results yielded the critical pressure of 1.5 bar for TUB 125 and 1 bar for BP 002-40 180 SDR 11 pipes, as shown in Fig. 6.29. The figure indicates the fact that the pressure range governing the arrest/propagation transition is wide, compared to the results of unslit pipe. This is presumably due to the much lower resistance of the material to the crack propagation in that having a sharp notch at the inner surface along the expected crack path drastically reduces material's crack propagation resistances. Additionally, due to the nature of RCP in the pipe, even for the test pressures lower than the critical value there is still a certain amount of driving force which drives the crack further. The similar wide transition pressure between arrest and propagation has been observed in some exploratory tests on PVC pipes using the S4 technique.

In Fig. 6.30, the variation of critical pressure results on inner surface slit 180 mm TUB 125 pipe with temperature is shown. The figure indicates that increasing the test temperature slightly increases the critical pressure, but compared to the results of unslit pipes, Fig. 6.12, this increase could even be negligible. Examination of the fracture surfaces of these inner surface slit pipes showed almost no ductility and not only eliminated existence of the tear lips on the inner fracture surface of the pipe but also, due to this, resulted in faster crack velocities and gave more brittle fracture surfaces. These rather interesting test results highlight the importance of the tear lips in the RCP phenomenon.

6.10. Diameter Effect

After extensive critical pressure definition tests on different grades of MDPE and HDPE 180 mm pipes the parameters and their influence on RCP performance were identified. If the S4 technique described in Chapter 5, is to be a serious contender as a type or quality control test, it must show a similar discrimination to pipe stock as is evidenced by the full-scale test. The ability to prove this has been limited by the need to ensure that identical grades of two contrasting materials are similarly extruded into pipe of 180 mm diameter for our own tests. For the same test material BG FS test results are only available for 250 SDR 17.6 and 11 pipes. This made it necessary to set up a new test rig for 250 mm diameter pipe. The results of these experiments are of particular importance in that they give one not only the chance to correlate the S4 and FS test results but also to measure the influence of diameter on the p_{CS4} results. An additional aim of S4 tests on 250 mm pipe was that after determining the correlation between FS and S4 tests, the actual FS p_c values for 180 mm pipe could be predicted.

Bearing in mind the above objectives, a series of tests was carried out on 250 mm pipes of 7D (1.75 m) length. S4 test results on 250 mm SDR 17.6 Finathene 3802 MDPE pipe, extruded by Stewarts & Lloyds (UK), at 0 °C are shown in Fig. 6.31 indicating the fact that the transition from arrest to propagation is taking place at a pressure range of 1.65 to 1.70 bar.

It has been known from FS tests results of BG, Fig. 1.5, that increasing the pipe diameter reduces the critical pressure. Additionally, the strain energy equation poses a dependence of;

$$p_c \propto \frac{1}{D^{0.5}} \quad (6.3)$$

Here, the dependence of p_{cS4} upon D tends, to some extent, to agree with the FS test results of BG in that increasing D reduces p_c . Nonetheless, the present results of tests on 180 and 250 mm pipes tend to give the correlation as

$$P_{cS4} \propto \frac{1}{D^{0.78}} \quad (6.4)$$

Bearing in mind the fact that the correlation given by Eq. 6.4 was obtained from only two different diameters, more tests on different diameters are required to confirm the trend in variation of critical pressure with diameter. Eq. 6.4 also confirms the decrease in BG test results of 125 mm and 250 mm pipes.

6.11. Conclusions

The experimental results from a novel small-scale test technique to study RCP in air pressurised MDPE and HDPE pipe have been summarised in this section. The results clearly indicate that the critical pressure, below which RCP cannot be sustained, is very well defined for all the test conditions considered. Nevertheless, these critical pressure results are remarkably low, a result which we attribute to the prevention of gas decompression.

These results show that the prediction of critical pressure by the Irwin-Corten theory is not straightforward, especially without taking into account the ratio of crack velocity to decompression speed. Preventing decompression of the gas and flaring of the flaps fixes the test boundary conditions very well, giving us the opportunity to check the reliability of this theory. Results indicate that the fracture energy per unit length is about 3 to 6 times higher

than the elastic energy stored per length of pipe wall. Clearly, as well as the strain energy stored in the pipe wall, energy stored in the gas also contributes to crack propagation.

The test results on different SDR values suggest that p_{CS4} values have a linear dependence on pipe thickness values as predicted by the Irwin-Corten equation but the rate of increase of critical pressure with thickness is only about 8% of that predicted from the equation.

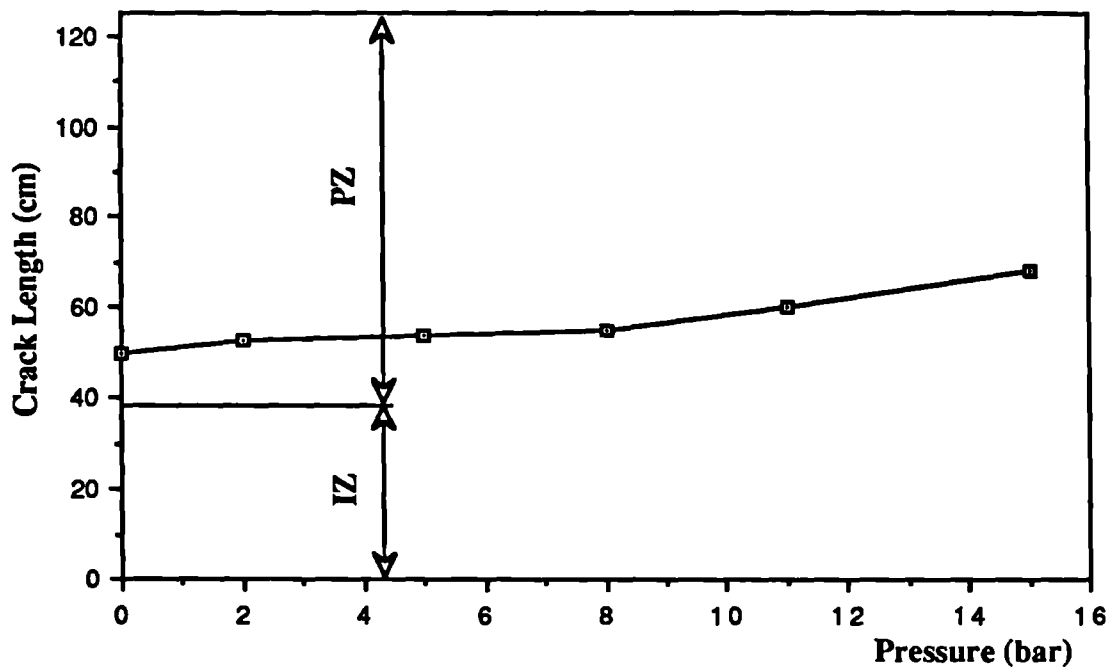


Figure 6.1 Effect of IZ pressure upon the final crack length for BP 002-40 180 mm SDR 17.6 pipe at -5°C

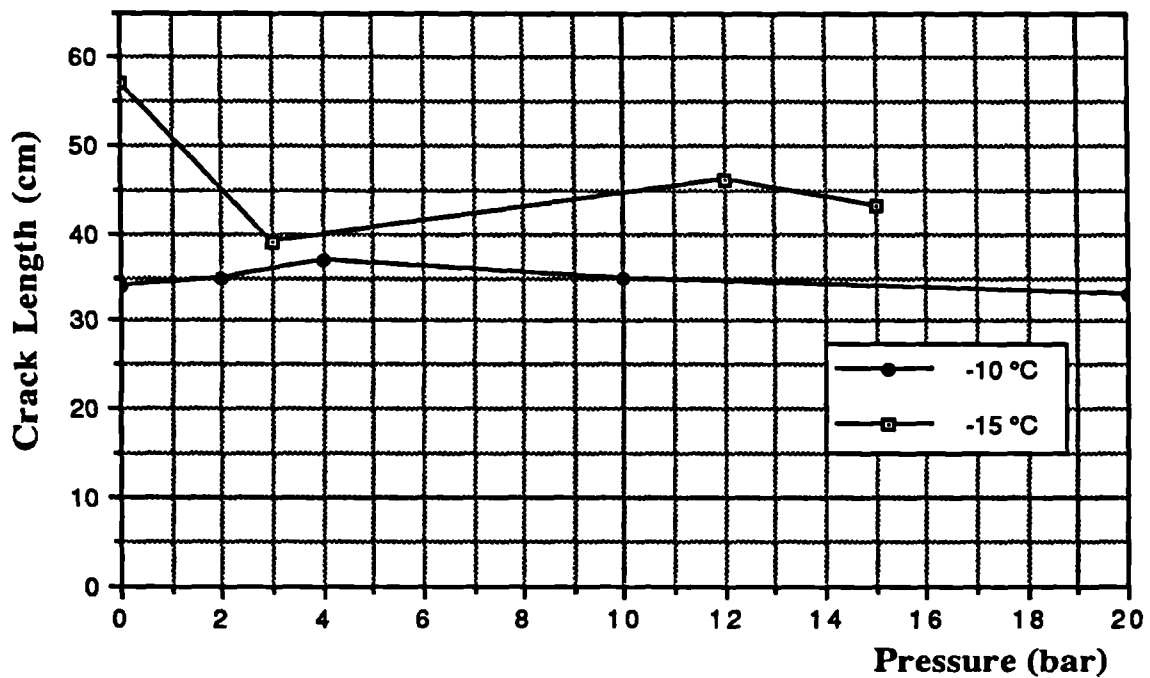


Figure 6.2 Influence of initiation section pressure on final crack length for TUB 125 180 mm SDR 11 gas pipe at -15 °C and -10 °C

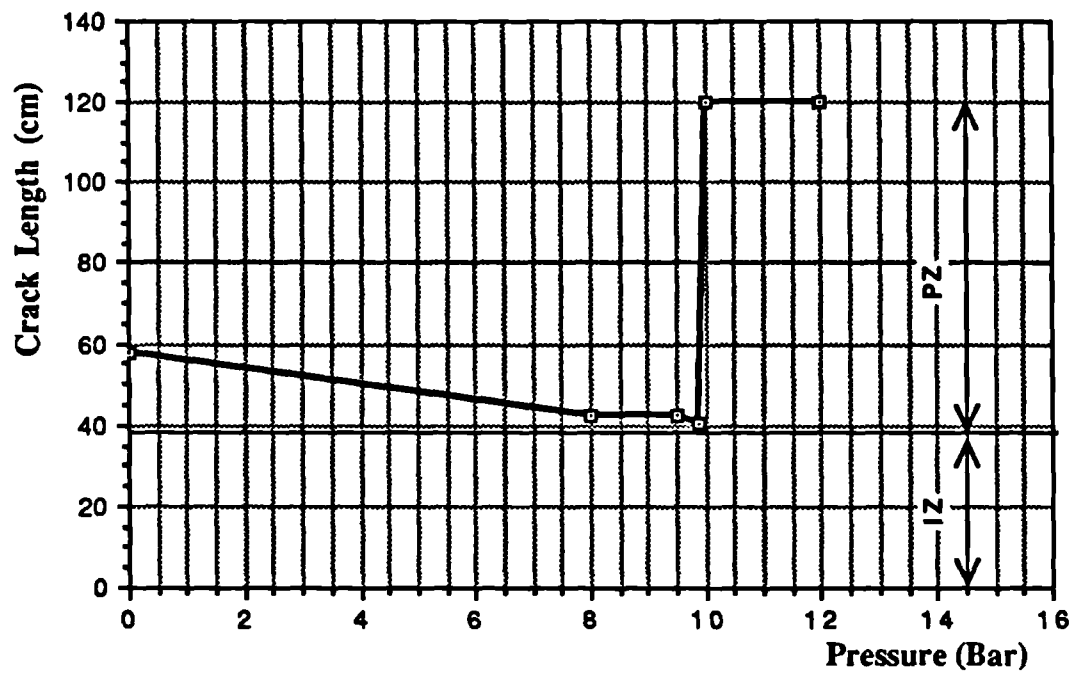


Figure 6.3 Variation of total crack length with PZ pressure for TUB 125 180 mm SDR 11 gas pipe at -15 °C.

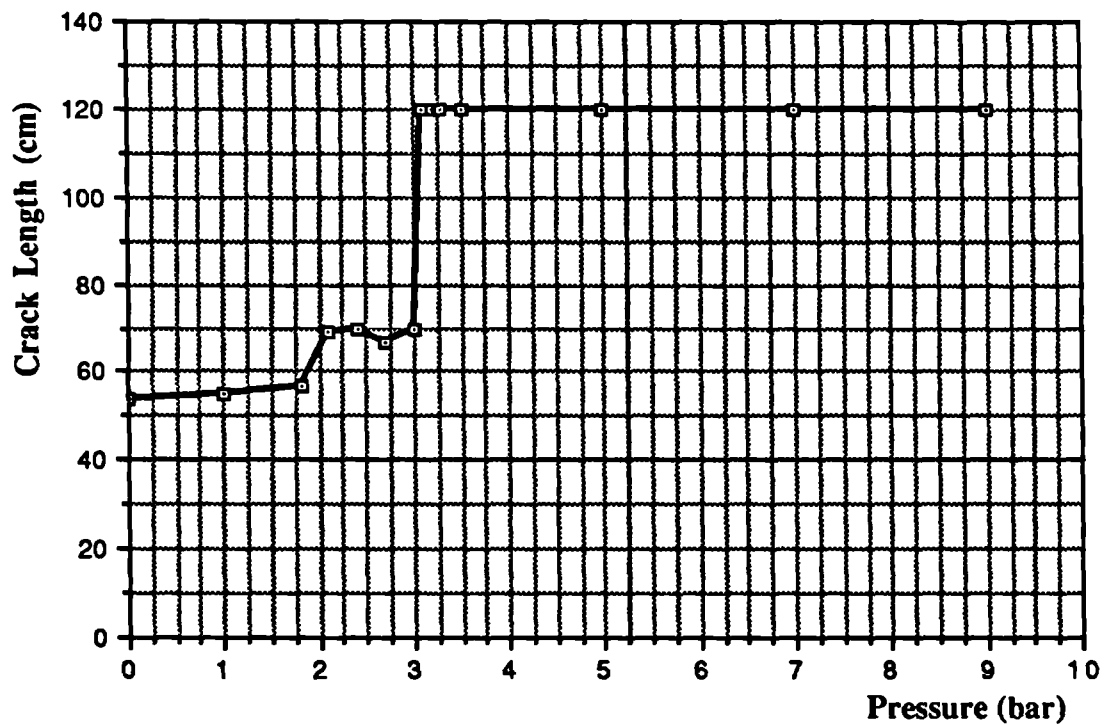


Figure 6.4 Variation of total crack length with PZ pressure for TUB 125 180 mm SDR 11 gas pipe at -30 °C.

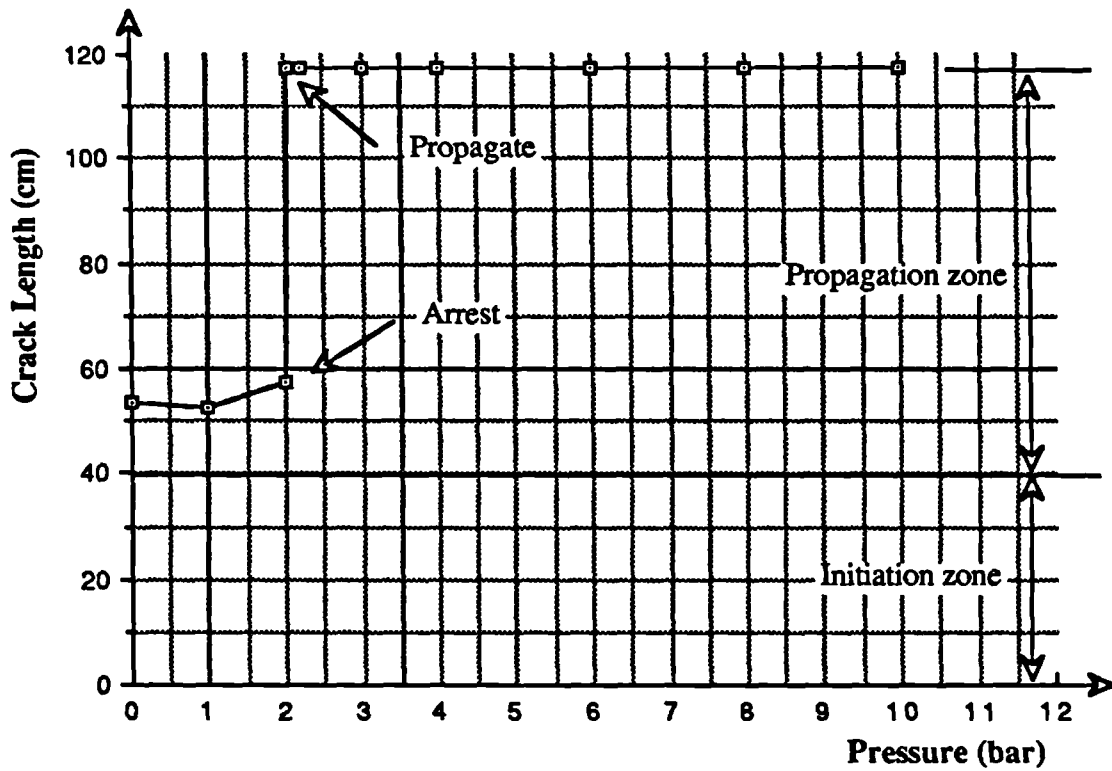


Figure 6.5 Variation of final crack length with increasing test pressure for BP 002-40 180 mm SDR 17.6 gas pipe at -15 °C.

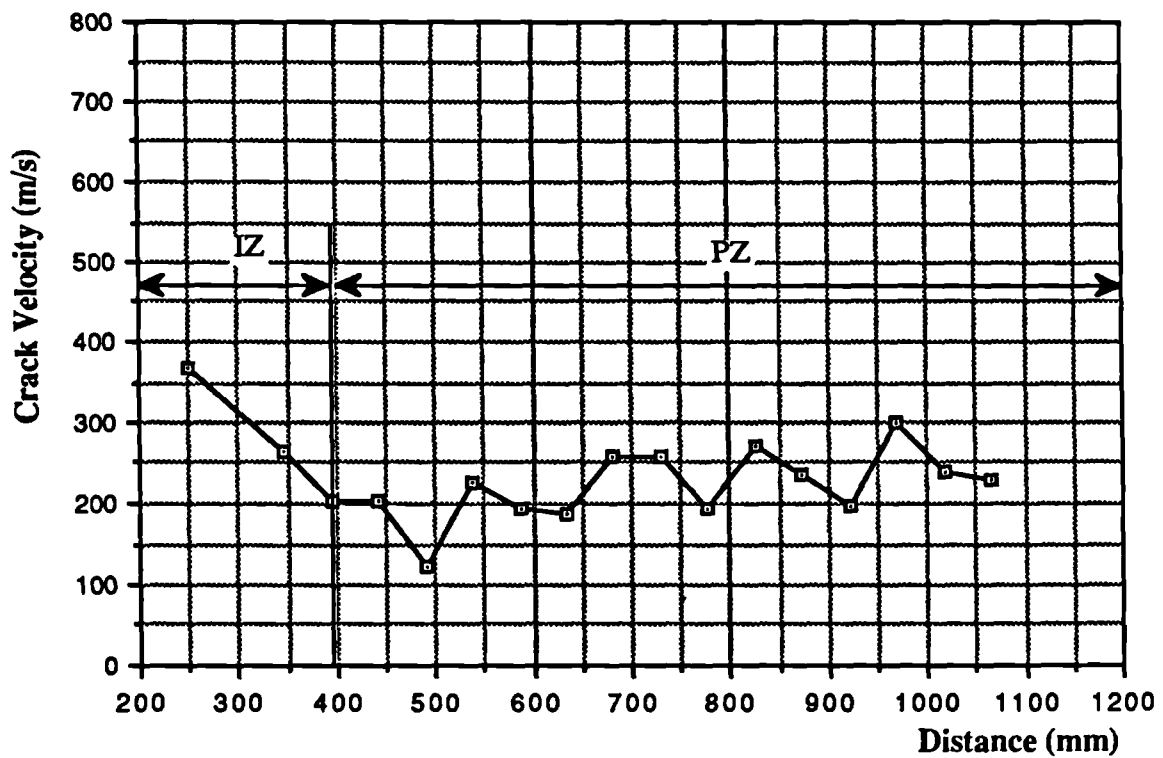


Figure 6.6 A typical crack velocity history at -15 °C and 4.0 bar pressure showing steady-state crack propagation along the propagation zone for BP 002-40 180 SDR 17.6 pipe.

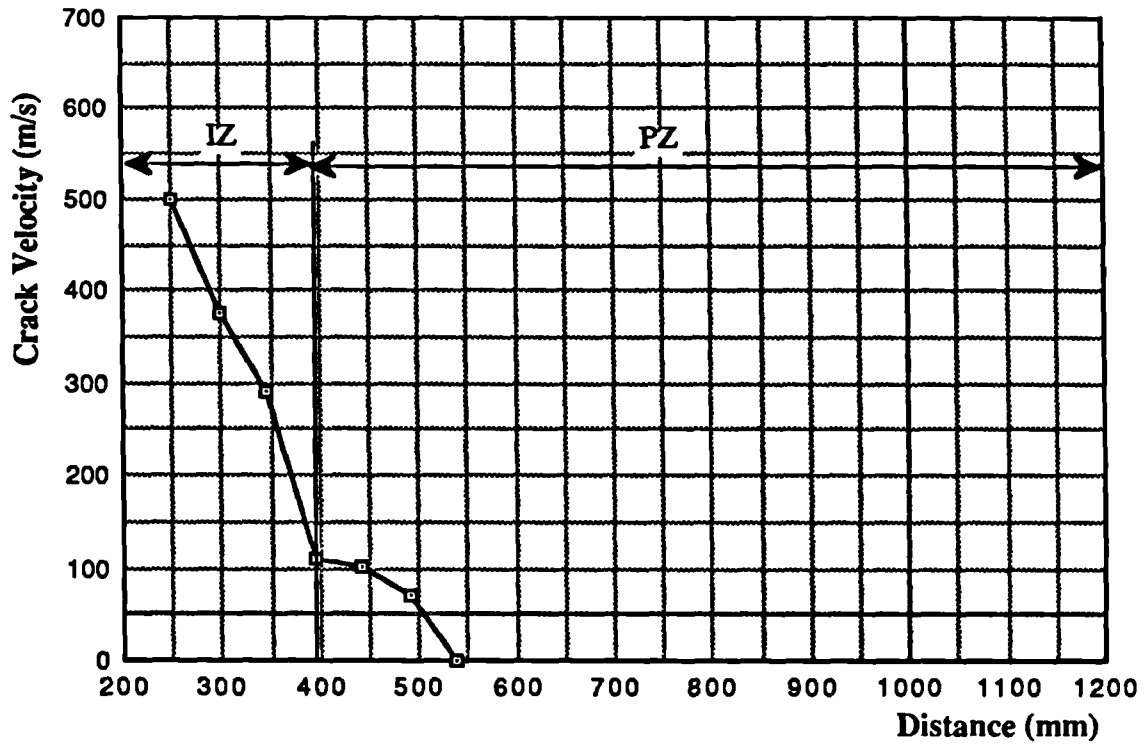


Figure 6.7 Crack length versus crack velocity for 2.0 bar pressure and -15 °C temperature, showing rapid arrest of the crack on entering the propagation section for BP 002-40 180 SDR 17.6 gas pipe.

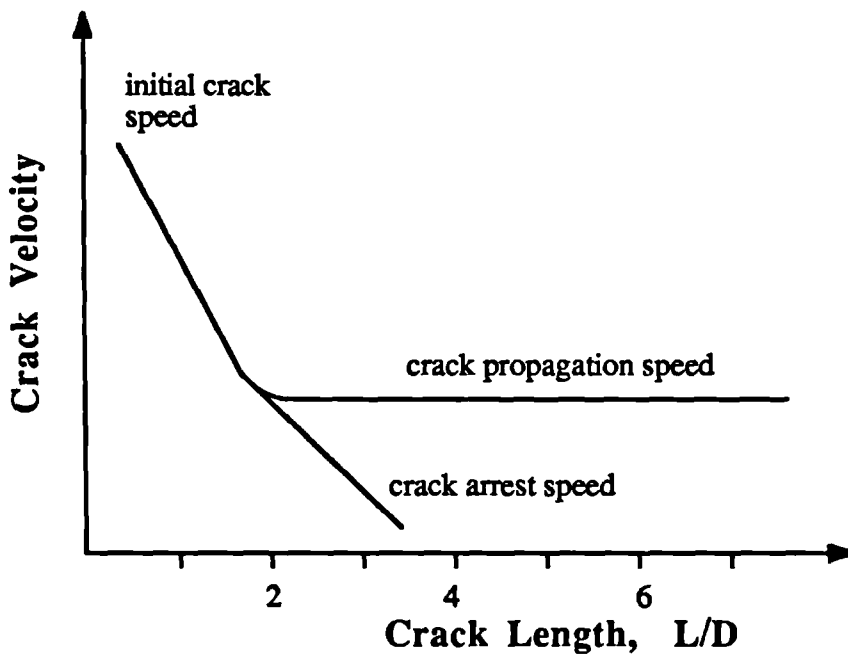


Figure 6.8 Schematic crack velocity histories distinguishing crack propagation and arrest behaviour.

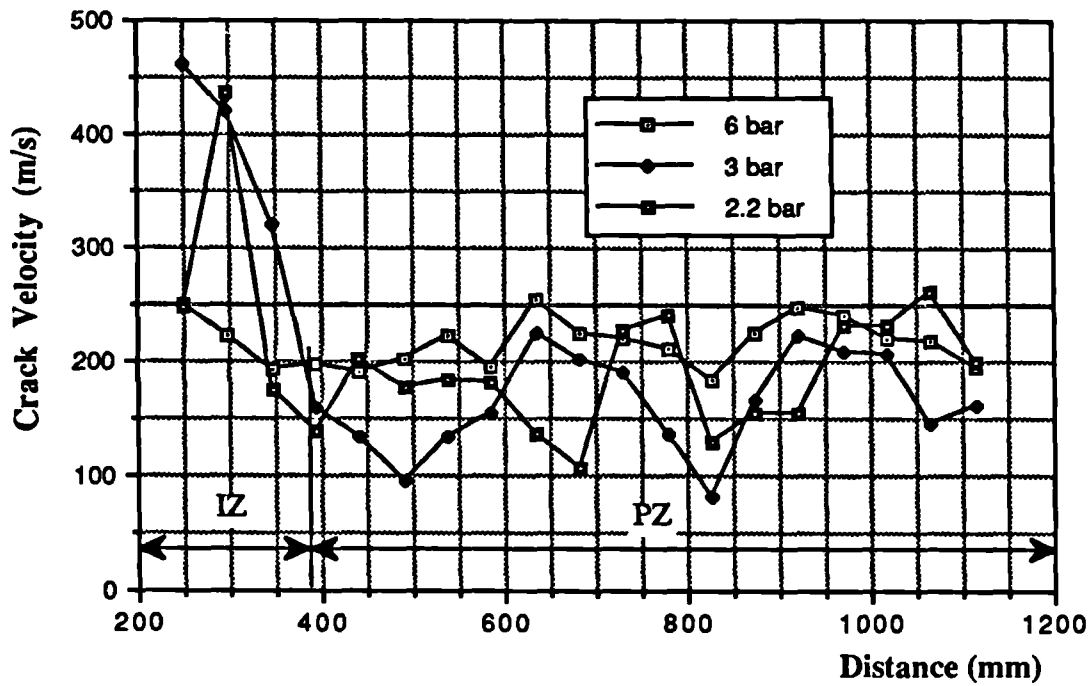


Figure 6.9 Crack velocity histories at -15°C for different test pressures for Du Pont Aldyl 'A' 180 mm SDR 17.6 gas pipe.

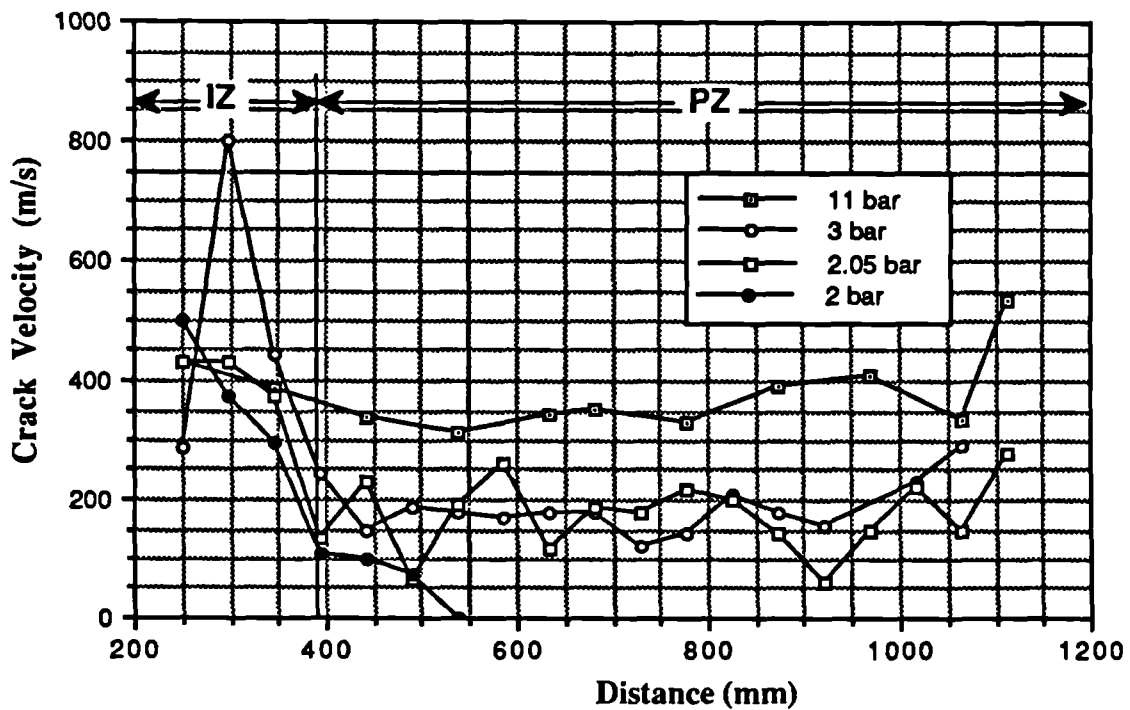


Figure 6.10 Axial crack velocity histories at -15°C for BP 002-40 180 SDR 17.6 gas pipe for different test pressures.

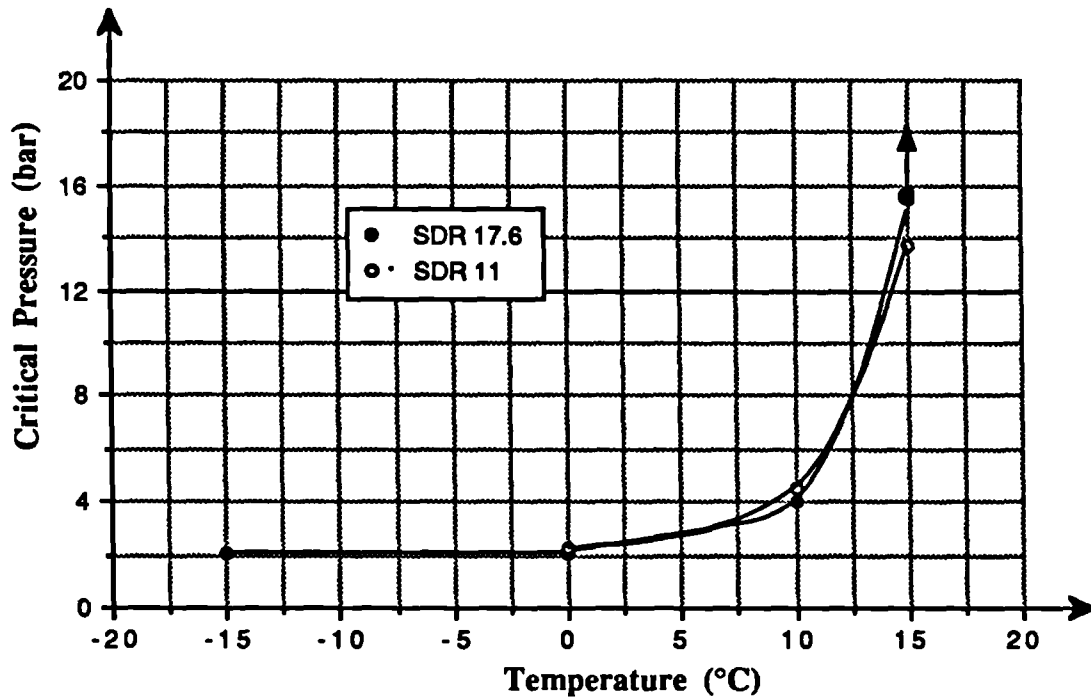


Figure 6.11 Variation of critical pressure with temperature, showing a rapid increase above 0°C and no crack propagation up to 15 bar at 15 °C for BP 002-40 180 SDR 17.6 and 11 pipes.

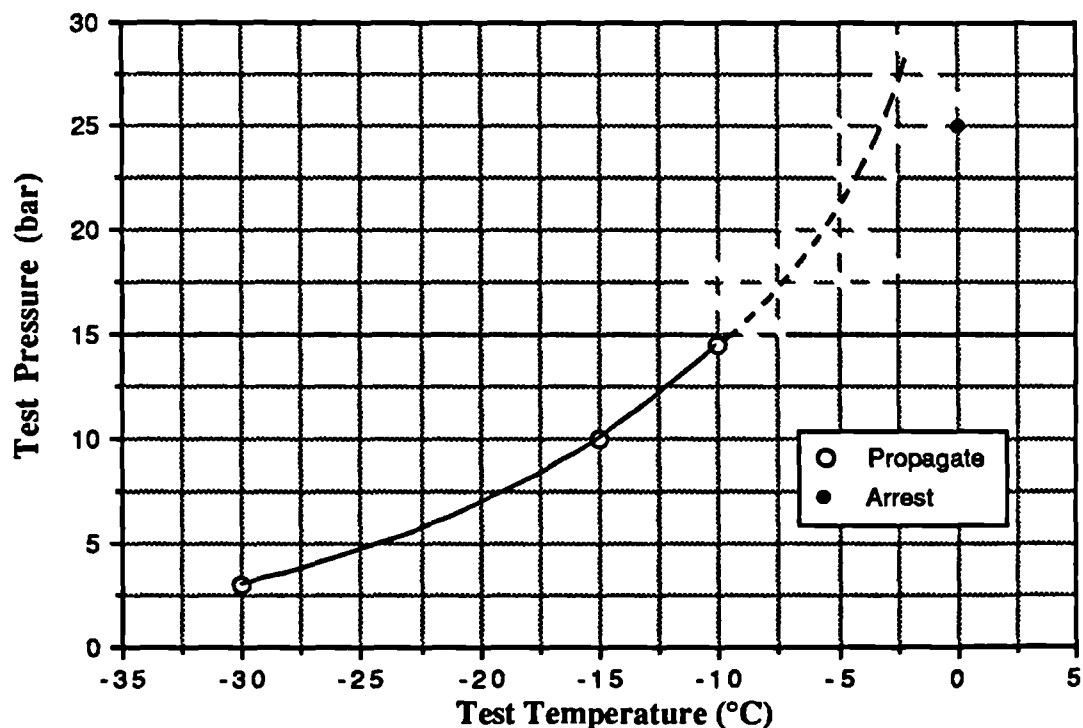


Figure 6.12 Variation of critical pressure with temperature, showing continuous increase between -30 and -10 °C and no crack propagation up to 25 bar at 0 °C for TUB 125 180 mm SDR 11 pipes.

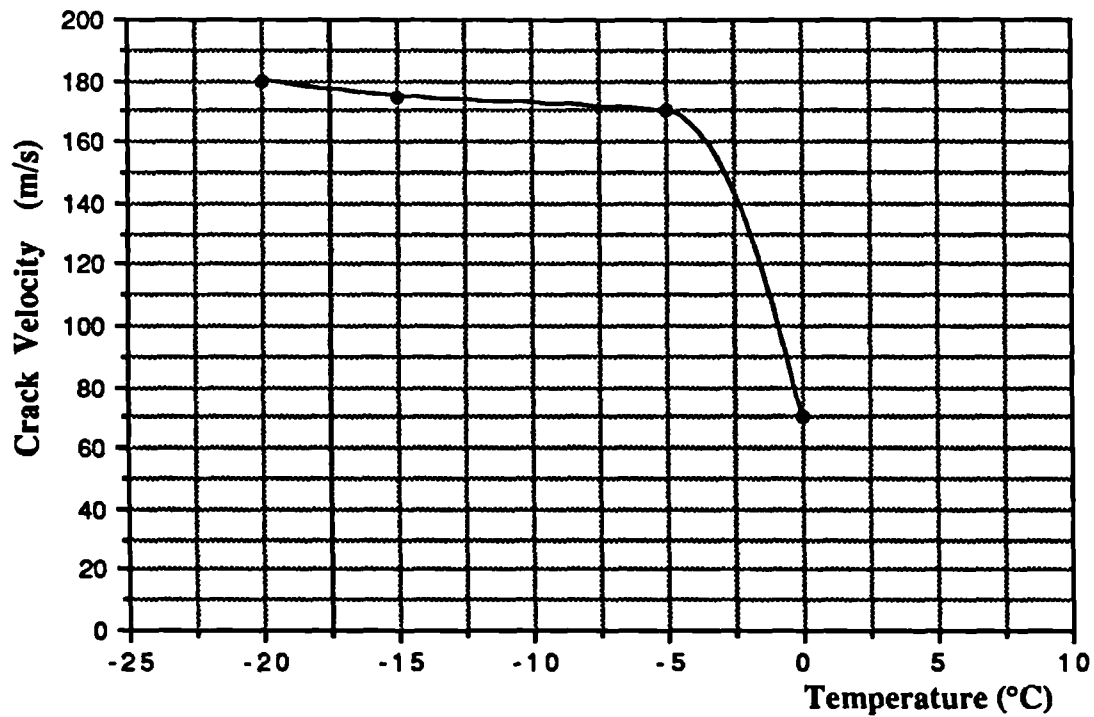


Figure 6.13 Effect of test temperature upon the average crack velocity for a constant pressure of 2.5 bar, showing relatively sharp drop above 0°C for BP 002-40 180 SDR 17.6 pipe.

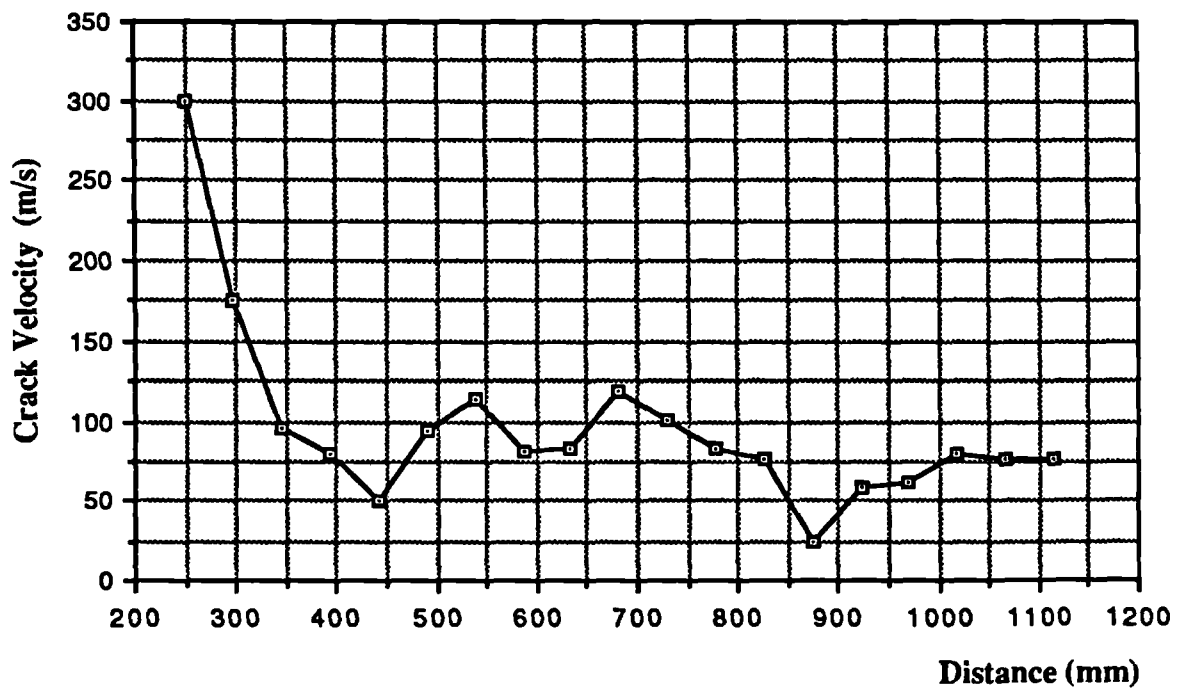


Figure 6.14 Crack velocity history along the pipe test section at 0°C and 2.5 bar for BP 002-40 180 mm SDR 17.6 pipe, indicating low crack velocity.

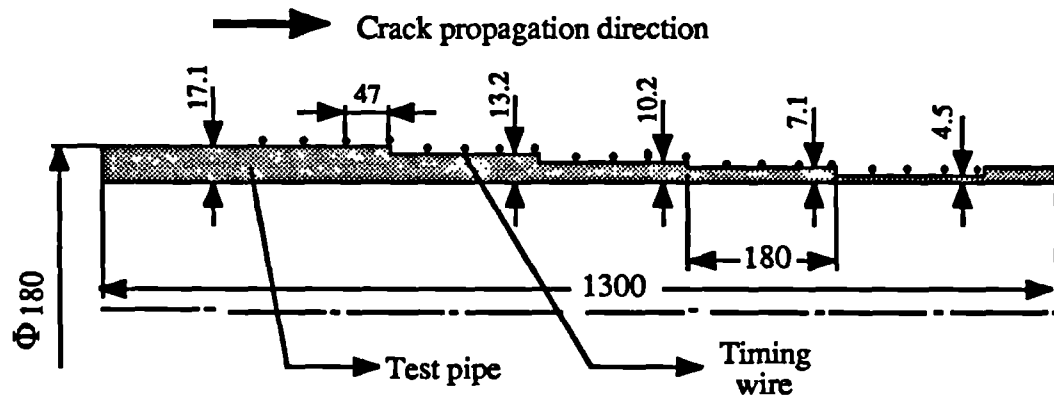


Figure 6.15 Schematic representation of the pipe having step-tapered wall thickness, showing the positions of the timing wires on the pipe.

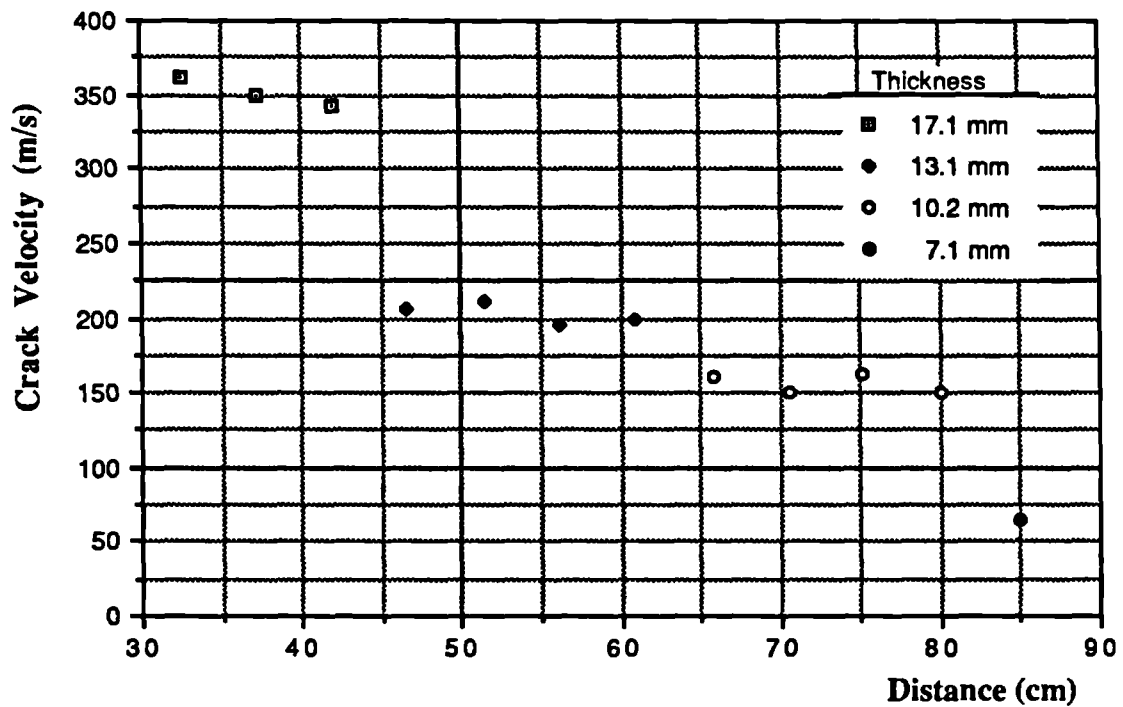


Figure 6.16 The crack velocity history along the step-like tapered section for 0 °C and 3.5 bar for BP 002-40 180 mm SDR 17.6 gas pipe.

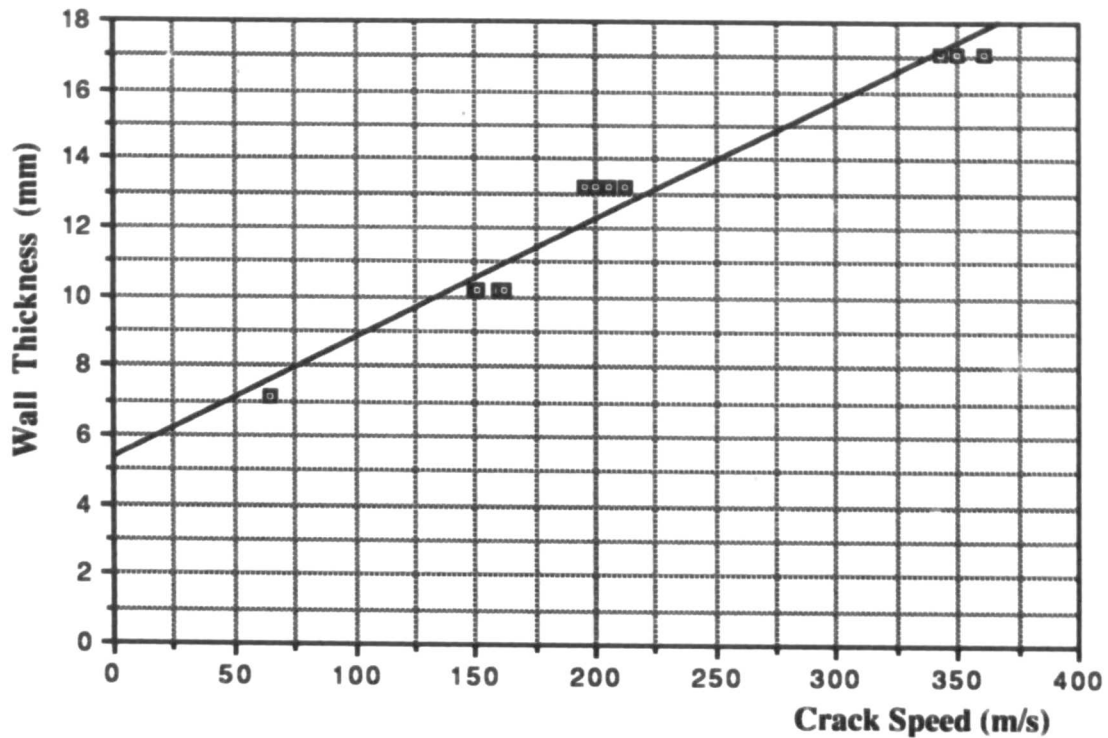


Figure 6.17 Influence of pipe wall thickness on crack velocity, showing almost linearly decreasing crack velocity with pipe thickness, at 0°C and 3.5 bar for BP 002-40 180 mm SDR 17.6 pipe.

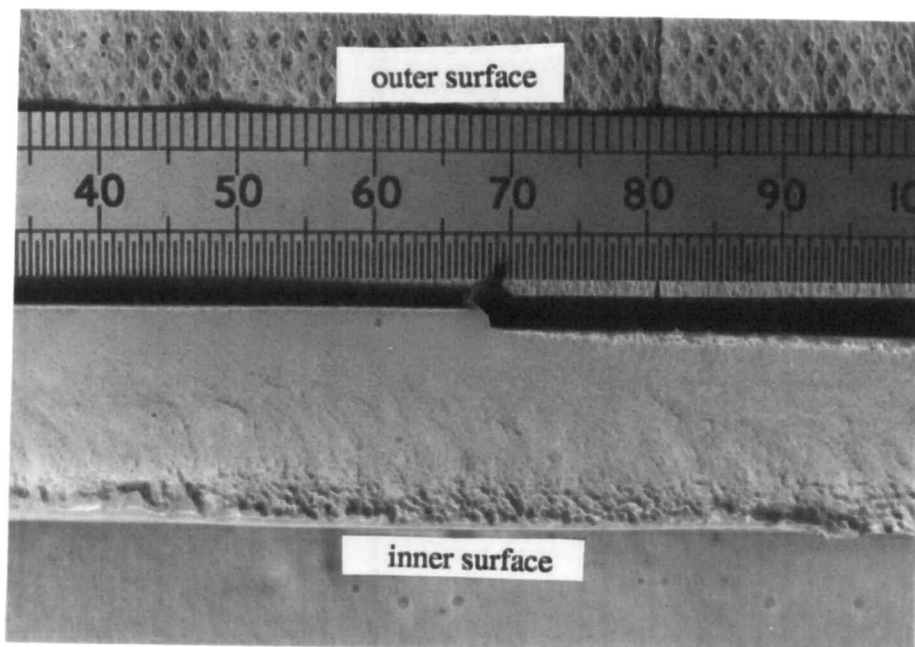


Figure 6.18 Crack propagation along a step-tapered 180 mm TUB 125 pipe tested at 12.5 bar and -15 °C whose thickness decreases in steps, showing a remarkable increase in ductility and finally arrest as the crack entered from 16.75 mm thickness to 15.35 mm thickness.

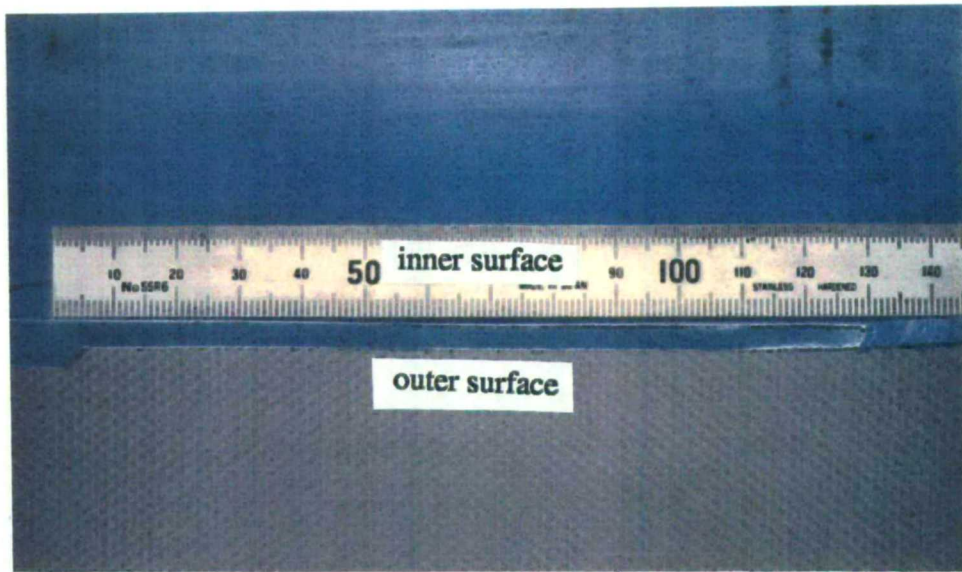


Figure 6.19 Crack propagation along a step-tapered 180 mm BP 00-50 pipe tested at 3.5 bar and 0 °C, showing crack arrest in the thinnest section of pipe wall.

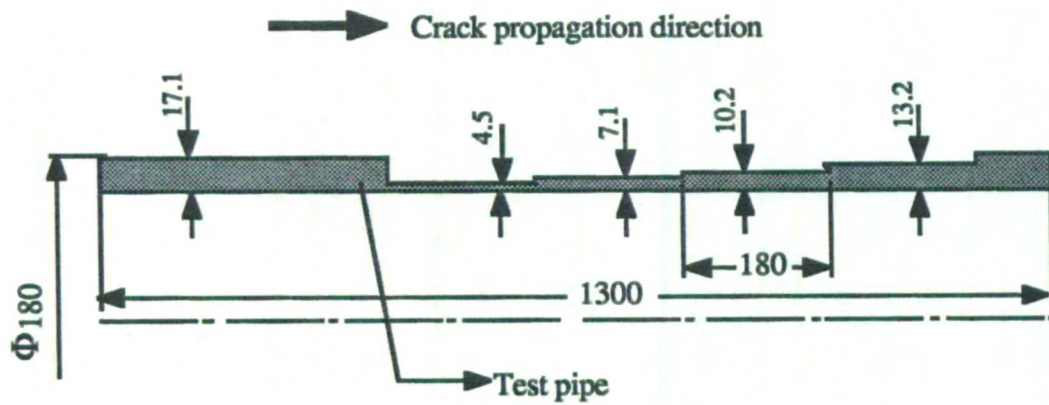


Figure 6.20 Schematic representation of the pipe wall configuration used to explore thickness effect.

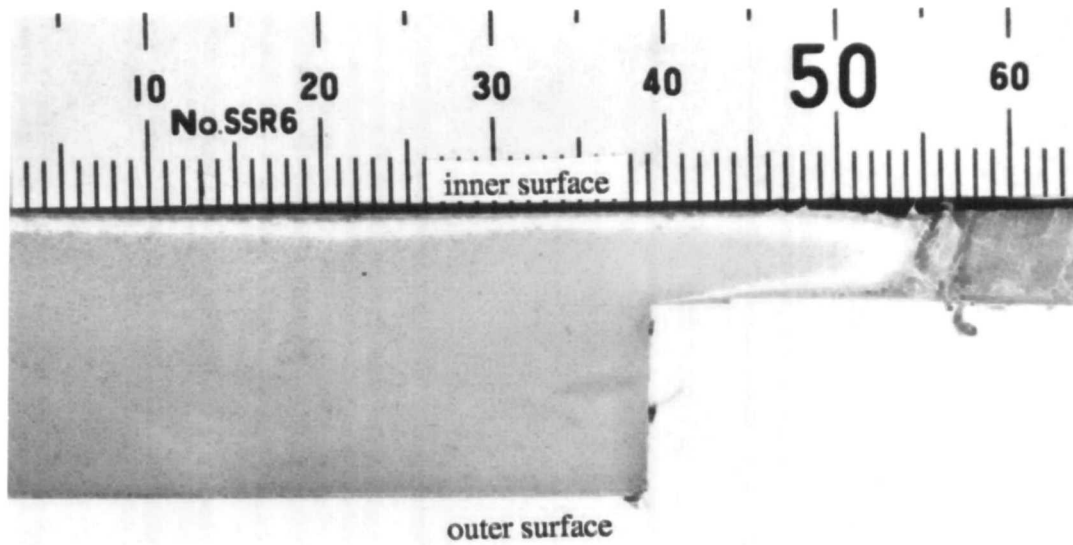


Figure 6.21 Crack propagation along a step-tapered 180 mm BP 00-40 pipe tested at 8 bar and 0 °C, showing crack arrest in the thinnest section of pipe wall (4.5 mm) and a remarkable increase in ductility as the crack entered from the thickest section (17.1 mm) to the thinnest section (4.5 mm).

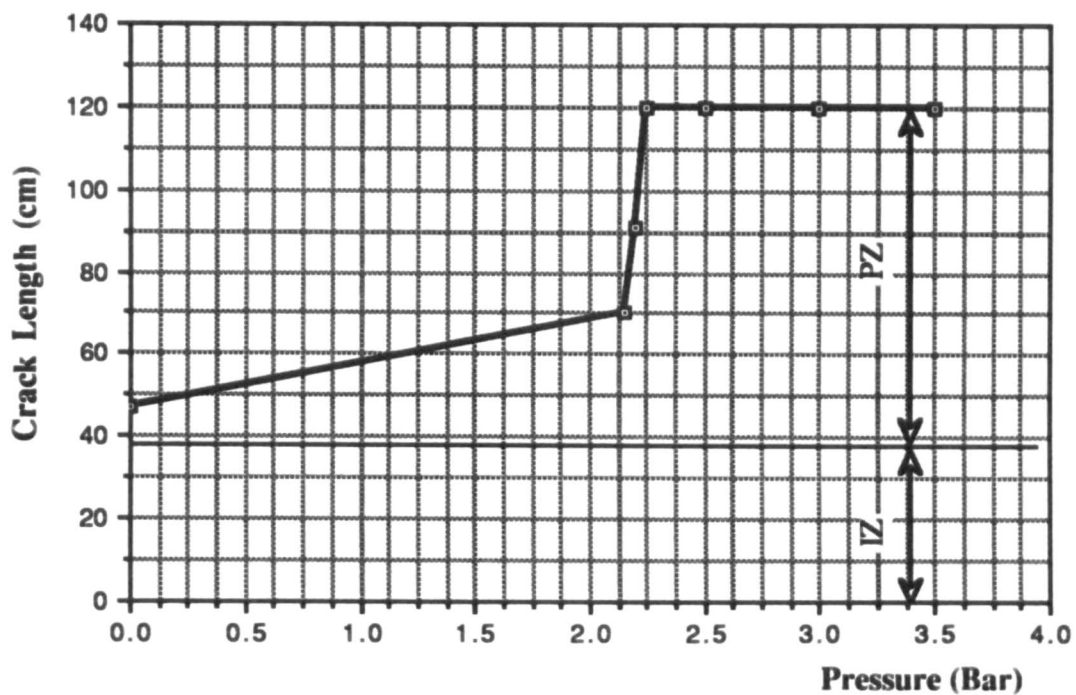


Figure 6.22 Critical pressure definition test results for BP 002-40 180 SDR 11 gas pipe at 0 °C, showing the critical pressure value of 2.25 bar.

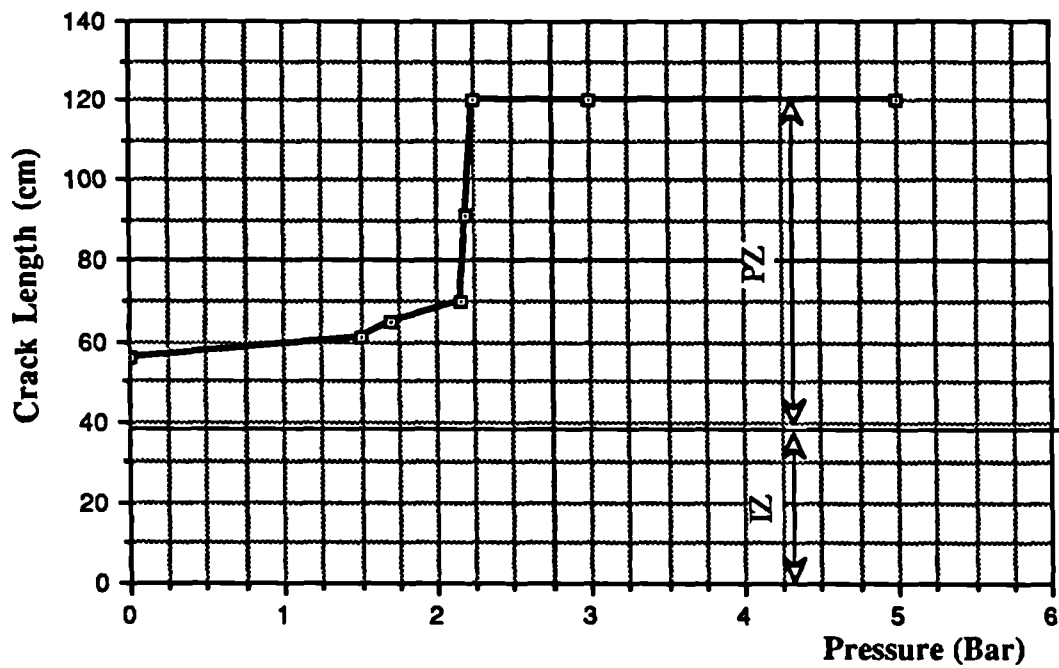


Figure 6.23 Critical pressure definition test results for BP 002-50 180 SDR 11 water pipe at 0 °C, showing the critical pressure value of 2.25 bar.

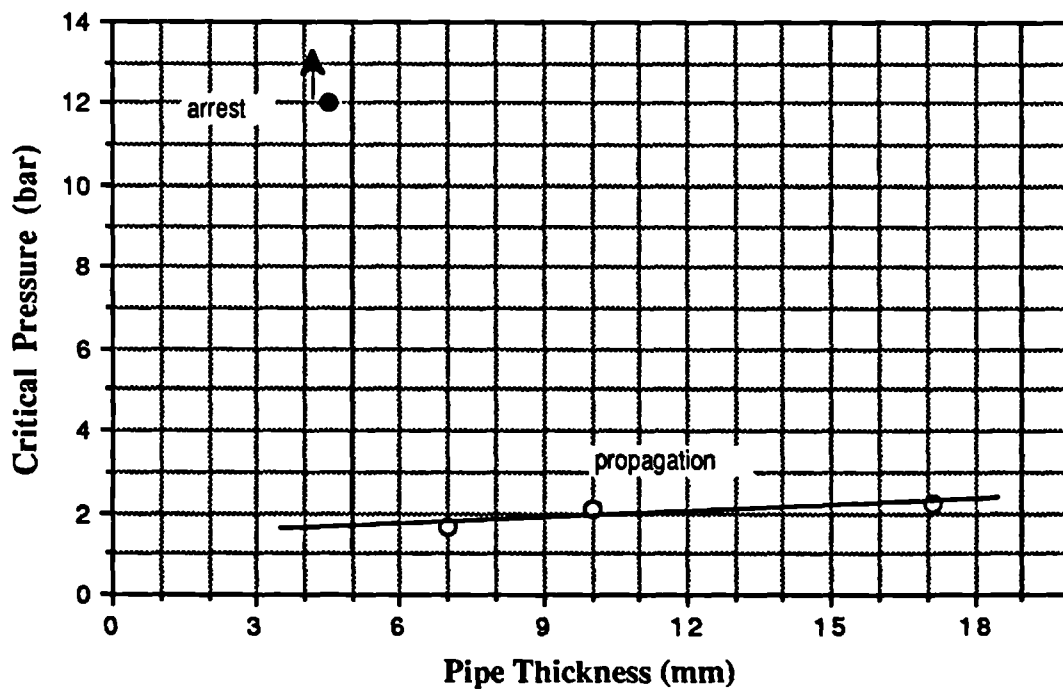


Figure 6.24 Effect of pipe wall thickness on p_{cS4} for 180 mm BP 002-40 MDPE pipe, indicating a 'critical thickness' between 4.5 and 7 mm.

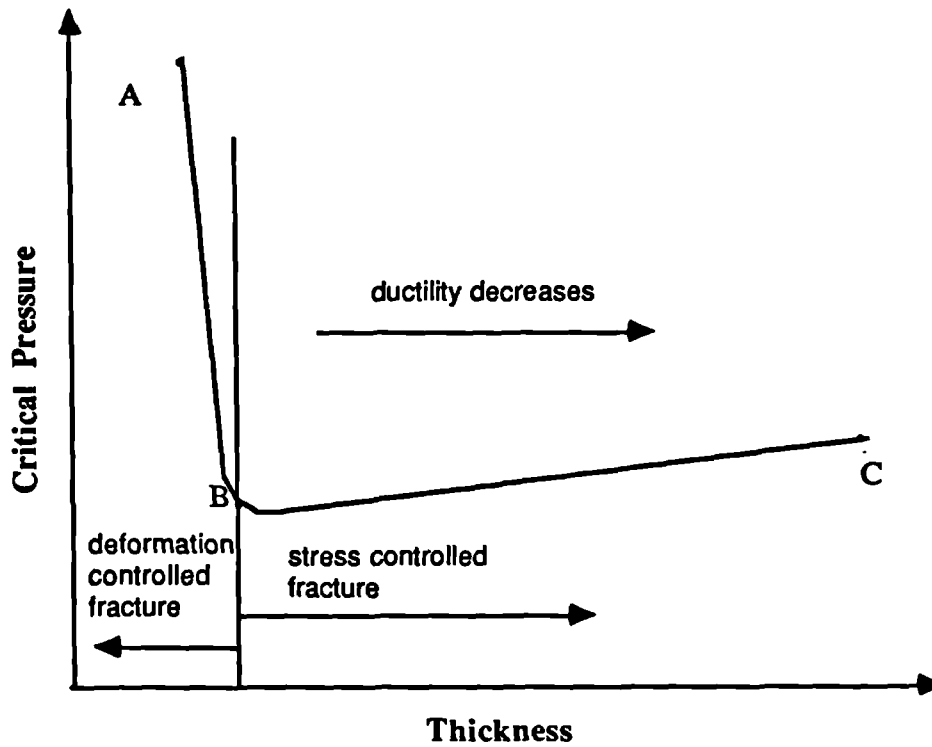


Figure 6.25 Schematic representation of thickness effect on RCP performance of PE pipes and damage mechanism controlling it.

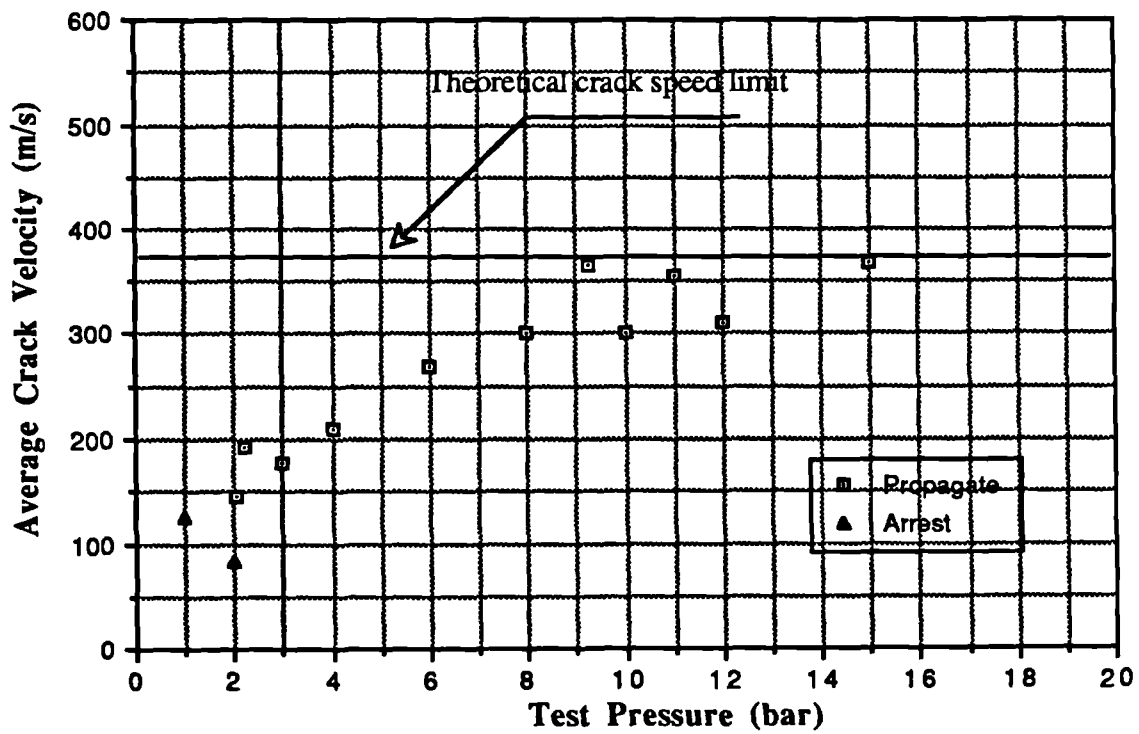


Figure 6.26 Variation of average crack velocity with pressure at -15 °C for BP 002-40 180 SDR 17.6 gas pipe.

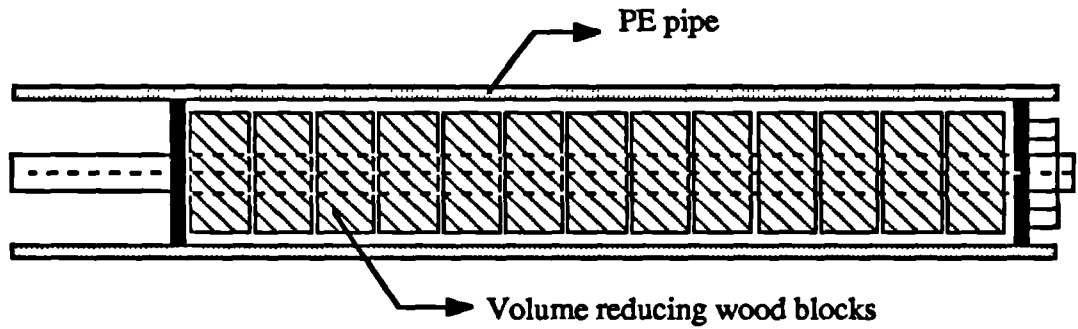


Figure 6.27 Schematic representation of the air volume reducing blocks used in experiments.

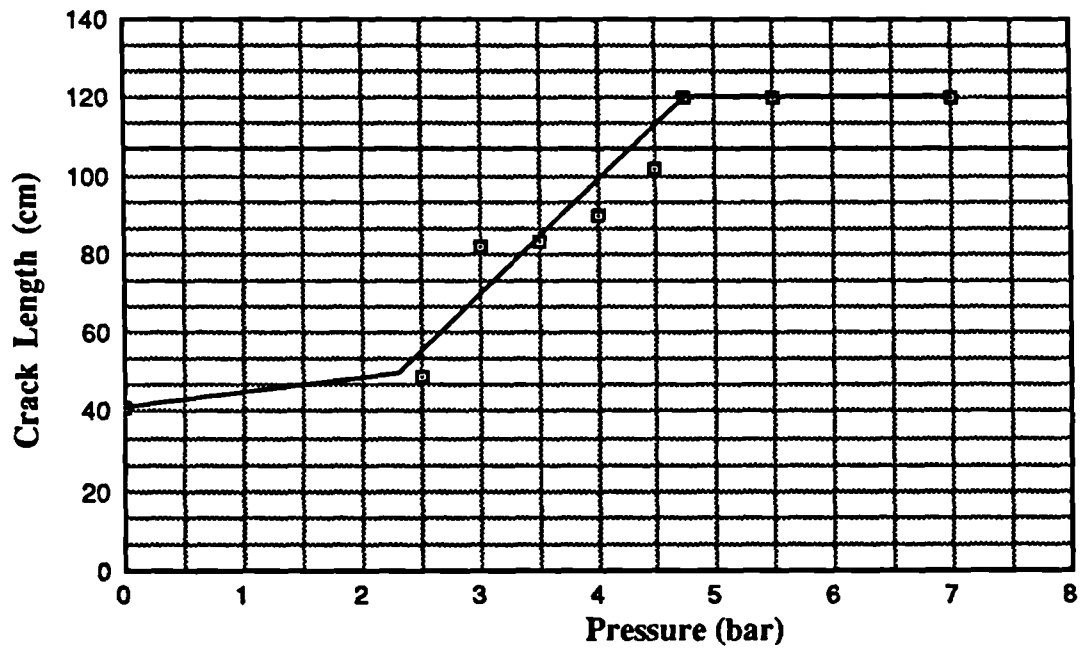


Figure 6.28 Variation of final length with pressure for BP 002-40 180 mm SDR 17.6 pipe at 0 °C for 12% air.

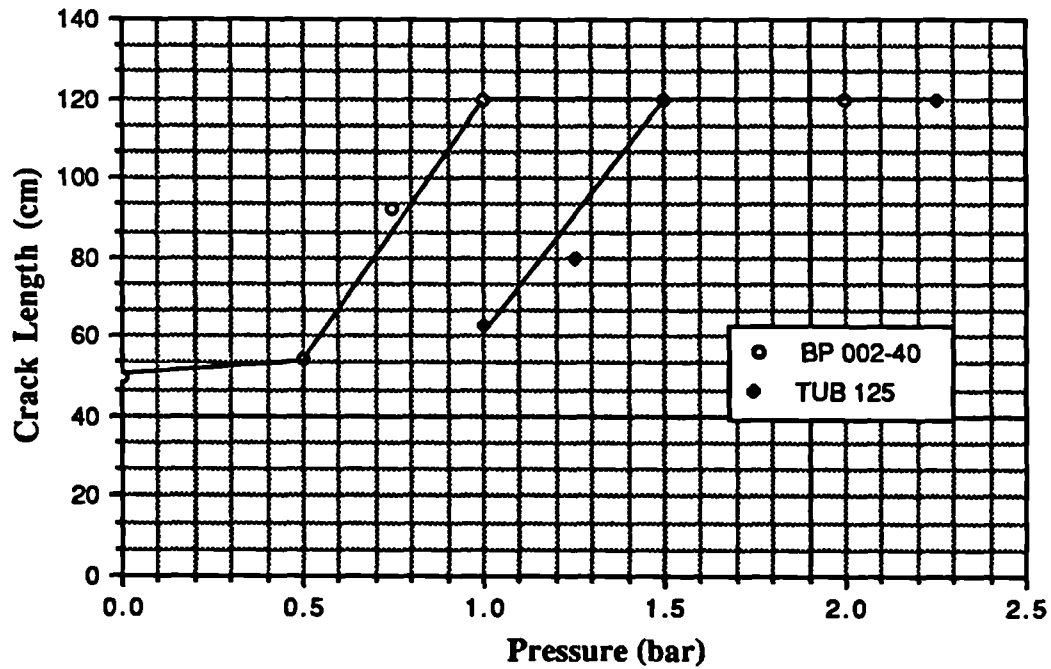


Figure 6.29 Critical pressure definition test results for inside slit 180 mm SDR 11 BP 002-40 and TUB 125 gas pipes at room temperature, showing the critical pressure value of 1.0 bar for the former, and 1.5 bar for the latter pipe.

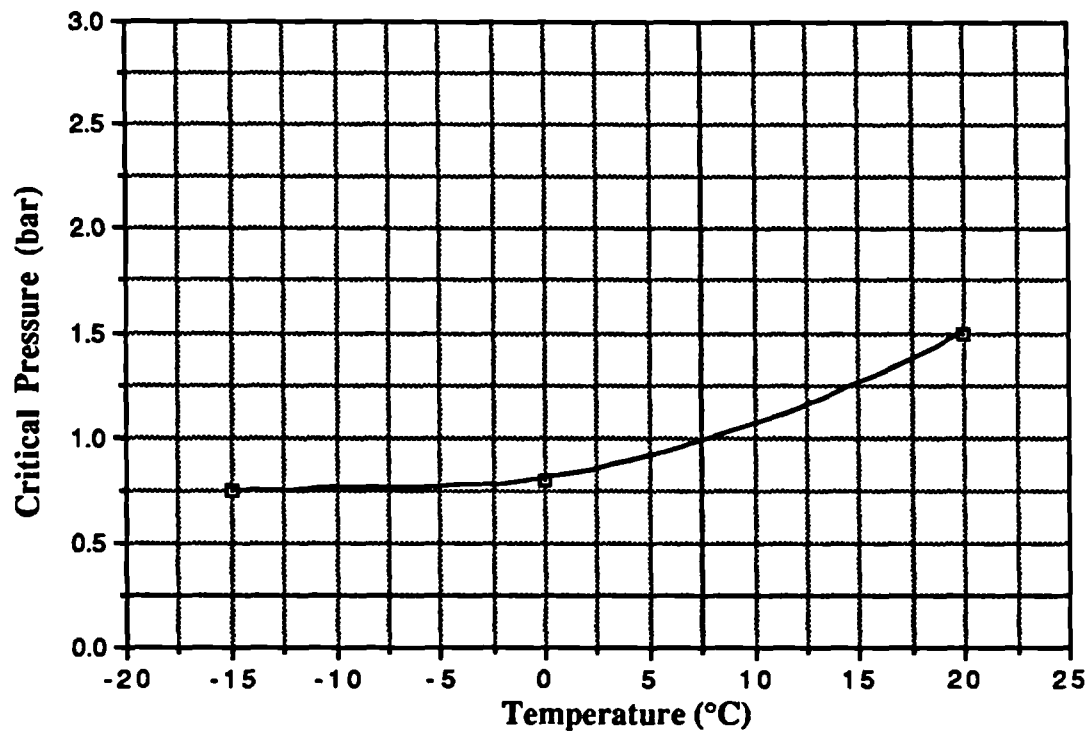


Figure 6.30 Variation of critical pressure with temperature for 180 SDR 11 TUB 125 in inner surface slit pipes.

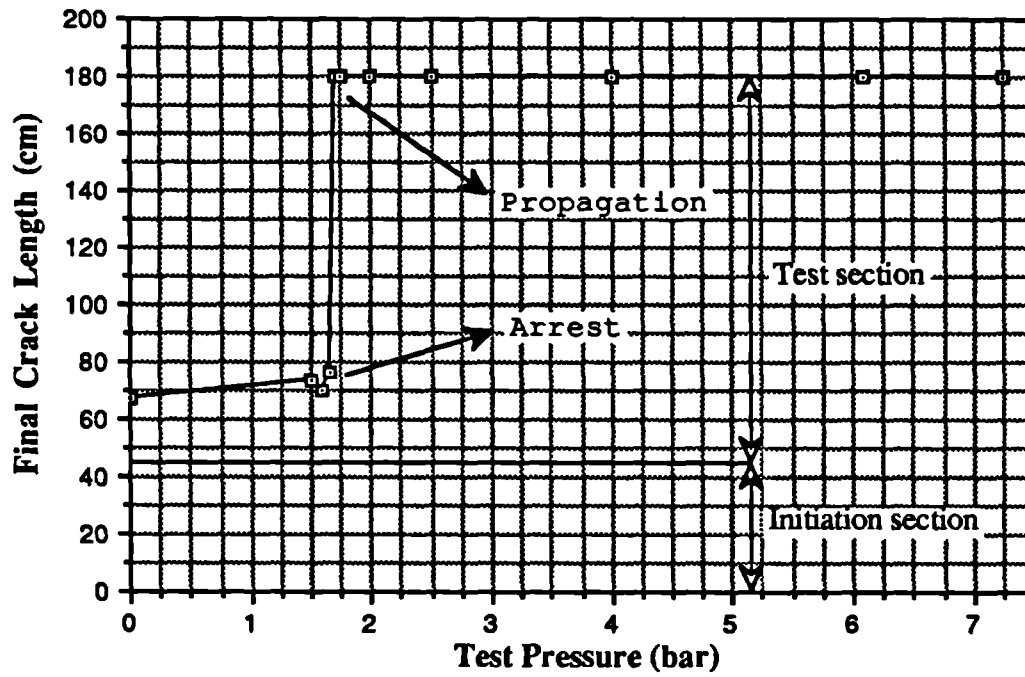


Figure 6.31 Critical pressure definition test results for Finathene 3802 250 mm SDR 17.6 gas pipe at 0 °C, showing the critical pressure value of 1.70 bar.

CHAPTER 7

MICROSCOPIC AND MACROSCOPIC FEATURES OF RCP IN PE PIPES

7.1. Introduction

Post-mortem examination of damaged or broken components at macroscopic and microscopic levels could be one of the most effective ways of examining the type and magnitude of applied stress state to the structure, and the response of the structure to it. In this Chapter, the characteristic features of RCP in PE pipe at micro and macro scales are highlighted. Particular attention is focussed on fracture surface analysis by optical and scanning electron microscope (SEM). Additionally, crack path instability and crack branching aspects of dynamic fracture mechanics are reviewed. Finally, the underlying mechanisms controlling crack branching and crack curving in PE pipes are studied and discussed.

7.2. Fracture Surface Characterisation of RCP in PE Pipes

7.2.1. General Remarks

Failure and fracture characteristics of polymeric materials depend upon their molecular structure. For that reason, studying the failure mechanics taking place during RCP is of special importance in understanding the nature of the fracture process and simulate the phenomenon. This microstructural study will enable one to understand better the effects of different parameters affecting the crack propagation as well as the critical energy release rate of the material.

Conventional fracture toughness tests hardly reflect the deformation mechanism taking place in RCP of PE pipes. Close fracture surface analysis of the broken pipes tested with the technique developed, clearly shows that both the macromechanism and micromechanism

involved in most RCP tests, particularly that in Charpy, are alien to RCP in pipes. This discrepancy of fracture process casts doubt about the relevance of these tests to real pipe failure. In order to define the actual fracture parameters of the material to be used in a model, a sophisticated test technique is required to take into account the deformation process as well as all the features to reflect the actual pipe deformation process.

Examination of the fracture surface of polymers, particularly PE, has undergone considerable attention for the last 20 years. As an initial study, Andrews (1968) studied the fracture surface of different polymers and presented the general markings seen on the surfaces that arise from the behaviour of fracture itself. Engel (1978) compiled a reference book from a variety of damage shown by polymers, using SEM in order to identify the type of damage and to characterise the underlying cause.

It is a conventional classification that fracture surfaces are divided into three, namely; brittle, ductile and mixed mode (brittle-ductile). However, the general characteristic micro features produced by fracture of polymers are quite different from those of metals. For brittle fracture, there is little or no plastic deformation on the macroscopic scale although the magnified view of fracture surface always shows that there is a certain amount of micro-ductility in different forms. Engel proposed a practical approach to divide fractures into two groups according to the lengths of the fibrils remaining on the surface:

Brittle fracture: fibril length 1-10 μm

Ductile fracture: fibril length > 10 μm .

Pointing out that although there is no brittle failure in polymers compared to the cleavage fracture seen in metals, Engel described the brittle fracture features as:

- deformed fibrils and films are limited to a thin layer (less than 1 μm)
- due to the small size of deformed volume of material on the surface the fracture surface is covered by oval torn-open blisters, steps and striped patterns
- since fracture proceeds in several planes, step-like patterns and chips are produced.

By applying the Dugdale model, Lee (1982) analysed the surface of slow crack growth in MDPE. He postulated that the resistance to crack propagation could be directly related to the amount of work needed in forming the observed fibres, and he argued that the length of the fibrils left on the fracture surface would be approximately half of the crack tip opening displacement. Thus, the following relation was proposed:

$$2 \Delta_f = CTOD = \frac{K_I^2}{\sigma_Y E} \quad (7.1)$$

Although the fibrils were longer than the predicted value, the equation could still be applied to field failures with satisfactory results.

Microscopic fracture surface analysis of the fractured samples seems to be one of the most effective ways of comparing the deformation mechanics and features of pipe tests and any fracture toughness test. This comparison can also be a powerful tool to decide whether the fracture toughness value obtained from the test technique may be used in modelling the pipe fracture event.

The fractographic study of RCP in PE pipes are aimed at;

- i) defining general features of fracture surfaces of the broken pipe and, as far as the fracture surfaces are concerned, establish the similarities and differences between the present small-scale test and full-scale tests;
- ii) studying the effect of pipe wall thickness and pipe diameter on the fracture morphology;
- iii) examining the variation of the surface features with temperature;
- iv) investigating the effect of material grade on the fracture morphology; and
- v) inspecting the variation of surface micro and macro features along the wavy crack path.

7.2.2. General Features of the Fracture Surface of RCP in PE Pipe

Studying the general features of the fracture surface of RCP in PE pipe at Imperial College was initiated by Strowbridge (1988). He compared the micro-fracture surface features of full-scale tests with the features from four laboratory tests: impact Charpy test, HSDT test, Glass-PE Duplex test and IC small-scale-steady-state pipe (S4) test. From this comparison he concluded that the S4 test indicates the most faithful recreation of fracture surface seen in full-scale tests. The Charpy impact, HSDT and Glass-PE Duplex tests all exhibited significant microscopic differences between their fracture surfaces. He has also studied the morphologies typical to RCP in full-scale PE pipes (Strowbridge, 1987).

Genussov (1989) continued SEM investigation of the microscopic aspect of RCP in PE pipe. Later, in collaboration with the author, Doughramachi (1990) continued this examination by using MDPE and HDPE gas and water pipes having different thicknesses and tested at various temperatures by the current technique.

Examination of the fracture surfaces of broken PE pipes was continued in more detail during the present work. It was shown that when the crack path was straight or nearly so, the fracture surfaces were, in general, very smooth with no sign of branching or under-cutting. On the other hand, there are a number of different fracture surface features when the crack follows a sinusoidal path. This is schematically illustrated in Fig. 7.1 and photographically shown in Fig. 7.2. The examination of fracture surfaces at different points around the sinusoidal crack path showed that surface features generally get rougher around the peak and more brittle and uniform through the thickness around the zone between peaks. Thus, when one wants to take a sample from that type of fracture for examination of different parameters, (*i.e.* thickness, grade, temperature, etc.) the location of sample on the sine-path has to be considered.

One of the major characteristics of the fracture surfaces is the tear-lips adjoining the inner and outer free surfaces, as schematically shown in Fig. 7.3. These tear lips appear as stress

whitened zones and when looked at by the naked eye it is easy to distinguish these regions from the other zones. The region between these two tear lips is quite brittle. Close examination of the surface at high magnification ($\times 1500$), shown in Fig. 7.4, indicates that there are a number of small dimples, less than $3\text{ }\mu\text{m}$ dominating the main fracture surface. In the centre of most of these dimples small inclusion like particles, less than $0.5\text{ }\mu\text{m}$, could be seen. Although there is no manufacturers' information about the nature of these particles, they could presumably be additives used for various purposes. The other striking feature of this section is the fibrils which are about $1\text{-}2\text{ }\mu\text{m}$ in diameter and $50\text{-}150\text{ }\mu\text{m}$ in length and elongated in the crack propagation direction. Thus, if this part of the region is examined under SEM it is possible to define what the crack propagation direction was. Similar fibrils have also been observed on the broken Charpy, HSDT and Glass-PE Duplex samples but the direction of these fibrils on pipe samples differs from the other samples.

The fracture surfaces of broken pipe sometimes indicate many parabolic and hyperbolic markings as shown in Fig. 7.5. The creation of these markings on the fracture surfaces are believed to be due to two distinct processes: firstly, showing not constant but varying magnitude of crack driving force. Secondly, there is a stress gradient through the thickness. The occurrence of these arrest marks reaches maximum if the test pressure is just above the critical value but decrease in numbers and magnitude as the test pressure is increased well above the p_C .

7.2.3. Thickness Effects

Using the present test technique it became possible to study the effect of varying thicknesses on the RCP performance of PE pipes. Since the variation of crack speed along the tapered pipe length has been documented in the previous chapter, here the variation of the pipe fracture surface for each thickness is studied. Macroscopic and microscopic inspection of the broken pipe have clearly highlighted some important features of RCP. One of the main characteristics of the effect is the variation of the tear-lips with thickness. The broken pipe surfaces for different thicknesses were examined and it has been observed that although for a

given temperature the width of the tear-lips at the inner section remains constant, the width of the outer tear lips decreases with increasing pipe wall thickness. In Fig. 7.6 the size of tear-lips at the outer and inner surfaces are plotted for BP 002-50 180 mm SDR 11 pipe, tested at 0°C and 3.5 bar. It is worth mentioning that as the crack entered the thinnest section (about 4.5 mm) it arrested. As seen from the graph, below a certain pipe thickness the size of the tear-lips at both outer and inner surface attains the same value.

The implication of the effect is that, for a given temperature, the speed of a propagating crack is controlled by the size of tear lips (or vice-versa) and, since the size of inner tear-lips is constant, it is the magnitude of the outer tear lips controlling the crack velocity and arrest behaviour. Furthermore, SEM examination of the surfaces revealed that for thinner sections the fracture surface is uniform through the thickness as shown in Fig. 7.7 (except the tear-lips at both sides). The uniformity of the surface through the thickness disappears with increasing thickness and it becomes more ductile at the inner part and more brittle at the outer. This through-thickness variation of fracture surface and the differences in the tear-lips size in the inner and outer section are mainly due to the stress gradient through the thickness. There are three reasons for the occurrence of this stress gradient:

a) Residual stress profile through the thickness: The subject of residual stress is discussed in detail in Chapter 3 and pointed out that during the extrusion of PE pipes a stress profile is created. The profile of the stress is generally compressive at the outer and tensile at the inner surface. The present experimental residual stress measurements and available literature on the subject indicate that the compressive stress at the outer surface could be as high as 2 MPa and the tensile stress at the inner surface could be up to 1.25 MPa. Thus, when a 180 SDR 17.5 PE pipe is pressurised at 2 bar the resultant stress (*i.e.* hoop stress + residual stress) at the outer surface becomes -0.24 MPa but the stress at the inner surface takes a value of 3.01 MPa. Accordingly, this final stress state is believed to have significant effect not only on the crack front shape (crack tunneling) but also the fracture surface morphology.

b) Stress profile created by the internal pressure (thick-wall cylinder):

The post-mortem investigation of the fracture surfaces showed that the ductility variation through the thickness became more striking with increasing thickness. This might also be explained by thick-wall cylinder theory. From Lamé's equation, the variation of hoop stress profile through thickness in a cylinder under internal pressure, p_i , could be written as;

$$\sigma_{\theta} = \frac{p_i}{k^2 - 1} \left(1 + \frac{r_o^2}{r^2} \right) \quad (7.2)$$

where r_o is the internal and r_i is the external radii of the cylinder, and $k = r_o/r_i$. From this equation, for any given internal pressure, the ratio of hoop stress value at the inner and outer surfaces of 180 SDR 11 and 17.6 pipe could be calculated as 1.25 and 1.13, respectively. On that account, increasing the pipe wall thickness could promote more ductility at the inner section than the outer.

c) Deformation of pipe flaps behind the crack tip: This process is believed to be the major cause of the ductility variation. It can be explained as follows: Due to the pressure acting on the pipe flaps behind the crack tip, the flaps deform radially as well as circumferentially. This deformation process creates both axial and circumferential stress gradients along the thickness which results in higher tensile stresses at the inner surface and smaller tensile or sometimes compressive stresses at the outer section of the pipe. Since the maximum deformation occurs at the top of the pipe (along the crack path) the stresses reach their maximum around the crack tip which results in the non-uniform ductility through the thickness. Ives *et al.* (1974) measured circumferential and axial strain at the inner and outer surfaces of steel pipe along the crack path. He observed the circumferential strain at the inner surface around the crack tip was 3500 microstrain but that at the outer surface was -1500 microstrain. This effect was also explored in quasi-static and dynamic FE analyses and the results are presented in Chapter 8. These results suggest that the hoop stress

at the inner surface could be up to an order of magnitude higher than the stress value at the outer section.

Variation of hoop stress through the thickness makes the state of stress at the crack tip triaxial. It has not yet been clearly defined how stress biaxiality at the crack tip affects the fracture performance of materials. This aspect is made more complicated by the triaxial stress state in the immediate vicinity of the crack tip. Despite these uncertainties, it is our conjecture that having the circumferential stress gradient through the thickness around the crack tip results in a non-uniform stress intensity at the crack tip leading to the ductility gradient on the fracture surface.

Non-uniform ductility on the fracture surface could also be influenced by the density (or crystallinity) profile through the thickness. During the extrusion process, the pipe is generally cooled from outside by a water bath or water spray and the outside surface cools down faster than the inside. This different cooling rate may affect the formation and the size of crystallites. Thus at high cooling rates nucleation tends to be sporadic and a relatively small number of large crystals form. On the other hand when the cooling rate is decreased many more nuclei form and a large number of small spherulites are obtained (Young, 1981). Thus, similar to the residual stress profile, a density profile is created in the pipe wall which could be defined by the degree of crystallinity through the thickness (Harget, 1989). This variation in density changes the micro-structure and alters the performance of the material in many aspects as pointed out in Chapter 4.

7.2.4. Temperature Effect

The effect of temperature upon the fracture behaviour of polymer is rather complex. This effect is well documented in Williams's book (1984), and Kinloch & Young's book (1985).

The influence of testing temperature upon the critical pressure for different pipe grades and thicknesses is presented in the previous chapter (Chapter 6). Herein, the influence of temperature is considered from a different perspective: fracture surface analysis.

The fracture surface for a given resin type changes significantly with temperature. This variation is similar to the change in fracture surface between different lots at the same temperature.

i) **Variation in macroscopic scale:** As the test temperature goes down, the probability of crack branching increases and thus, generally, the crack follows more than one path. The hairline path of RCP becomes more wavy and crack propagation paths tend to follow a non-uniform direction. If the test pressure is close to the pipe's p_c value, a number of crack arrest marks could be seen on the fracture surface, as shown in Fig. 7.5. Increasing the test pressure reduces these crack arrest marks in number until a point where any further increase in the pressure suppresses the occurrence of these arrest marks and forces the crack to propagate in a straight manner. The second apparent effect of decreasing the test temperature is that deeper undercutting takes place with increasing amplitude (the effect of temperature on crack oscillation and branching is studied under the subject of 'crack curving and branching' in the following section). The additional effect of temperature on fracture surface at a macroscopic level is that the lower the test temperature, the more likely the crack is to propagate with a curved front, as schematically shown in Fig. 7.8. Through the inner third of the section the crack front advances mainly in the axial direction, except for a very narrow band close to the inner surface (due to plane-stress effect). It is likely that this propagation process is driven by the circumferential strain energy stored in the pipe wall. Over the remaining surface, crack propagation is mainly towards the outer edge. The commencing of the second type of propagation is controlled by bending due to pressure. If the test pressure is just above the p_c , the fracture surface analysis indicated that the starting point of this mode could begin from a third of the thickness from the inner surface. However, this point shifts toward to the outer section with the increasing pressure. It is also believed that the driving force for this propagation mode is not the strain energy in the pipe but is supplied by the pressure acting on the flaps behind the crack tip.

ii) **Variation in microscopic scale:** In order to observe the morphological changes at a microscopic scale, a number of fracture surfaces cut from different pipes tested at different test temperatures were examined under the SEM and optical microscope. The general trend is that the higher the temperature the higher the micro ductility on the surfaces. Additionally, the size and colour of stress-whitened tear lips, particularly at the inner side, become more apparent with the increasing temperature. The RCP performance of polymers, like most engineering materials, deteriorate as the temperature is decreased. This is because of an increasing tendency for crack tip blunting with increasing temperature arising from the temperature dependent decrease in yield stress. Furthermore, due to a decrease in modulus with increasing temperature the stress wave velocity diminishes which causes the crack to propagate slowly, allowing more time for the huge stress field at the crack tip to spread out to the region surrounding the crack tip and increase the depth (or volume) of the material undergoing temporarily visco-elastic and, to some extent, permanent plastic deformation underneath the fracture surface. If this damaged volume could be measured accurately, it is believed that, this would be most effective parameter in fracture toughness definition and could assist interpretation of the fracture process (particularly the molecular theory of RCP).

7.2.5. Material Grade Effect

It is worth pointing out that the temperature, below which the morphological features mentioned above occur, is very much material dependent. For TUB 125 the temperature above which RCP could not be observed at any pressure up to 25 bar is 0 °C and thus the transformation from brittle to ductile state is between -10 and 0 °C for 17.1 mm thickness. Nevertheless, for BP 002-40 the temperature is between 13 and 15 °C for the same thickness. Consequently, although for these two materials the maximum temperature above which RCP could not be generated differs, the variation of fracture surface features with temperature are similar. Another observation to be pointed out is that depending upon pipe grade the

appearances of the fracture surfaces at this maximum temperature, above which all the attempts to result in RCP failed, differ from each other in many aspects. For TUB 125, as shown in Fig. 7.9-a, the fracture surface is much more ductile and stress whitened, which is visible to the naked eye, and the ductility on the surface diminishes gradually from the inner to the outer section. For BP 002-40, on the other hand, the magnitude of both ductility and the stress whitened zone is much smaller and the transition from the ductile inner surface to the brittle outer surface is much sharper, as shown in Fig. 7.9-b. Thus, pipes made of different grades having the same p_c value (test conditions could be different) do not necessarily have the same fracture surface morphology. However, by looking at the fracture surface, an experienced person on RCP PE fracture surfaces would have some qualitative idea of the p_c value for given test conditions. However, the use of this fracture surface roughness as a tool for quantitative fracture toughness prediction seems to be not so easy. Additionally, for a reliable fracture toughness prediction from the fracture surface, it is believed that the volume (or depth) of the deformed zone underneath the fracture surface has also to be considered.

7.2.6. Slitting Effect

The effect of slitting of pipe along the prospective crack path upon fracture surface morphology was studied in two different ways. In the initial experiments a razor blade slit of 1.5 to 2 mm in depth was introduced on the outer surfaces of the pipe in the axial direction. In the experiments carried out on MDPE and HDPE pipes it was observed that the initiated crack did not follow the slit but either deviated or oscillated for any pressure and temperature. In the second experiments, the test pipes were slit on their inner surfaces along the axial direction. These tests indicated that having a slit on the inner surface is more likely to cause crack propagation along the prospective path mapped out by the slit. The significant outcome of these test was that, the inner slit along the inner surface eliminated not only the tear-lips on the inner section but also the stress whitened zone close to it, as shown in Fig. 7.10-a. After observing significantly lower critical pressure on these tests, it is well understood that the source of high crack propagation resistance for both MDPE and HDPE clearly rests in the

ability of the the tear-lips and the stress whitened zone close to it to absorb energy. Having the inner side slitting removed the plane-stress region on the inner surface thus markedly increasing the length of the parabolic crack front shape, as shown in Fig. 7.10-b.

7.3. Crack Curving and Branching

7.3.1. General Background

In brittle materials, such as rock, some polymers and metals subjected to dynamic loading, a propagating crack may deviate from its original straight trajectory and curve.

Ravi-Chandar & Knauss (1984 a-b) demonstrated a running crack having an oscillatory crack path of continuous curves due to interaction with low amplitude stress waves. Their specimens were rectangular in shape and had different widths. It was assumed that when stress waves are reflected from the boundaries they return to the crack tip and alter the crack tip stress state and this results in a variation in crack velocity; or, if these reflected stress waves are very high this causes branching. Another important fact pointed out by the same authors is that crack curving depends strongly on the specimen geometry and loading conditions. In order to study this effect they conducted experiments on specimens of symmetrically (dimensional) decreasing size. In smaller (narrower) specimens the main crack followed an oscillating crack path, whereas the same tests carried out on larger (wider) specimens under the same loading conditions, led to a straight crack path along the axis of symmetry. These differences in crack paths were proposed to be entirely due to differences in the stress field caused by the number of stress waves reflected from boundaries. In these tests another interesting result is that the crack velocity varied along the curved path between 350 to 450 ms⁻¹. The variation was argued to be due to the wave interaction. However as mentioned by Knauss & Ravi-Chandar (1985), the details of the wave history at the crack tip were not clearly enough established to make a conclusive statement about the effect of such an interaction of the crack tip with stress waves.

RCP in pipes is a continuous process and it is difficult to explain that crack curving is due to stress wave reflection from the specimen boundaries. It has been well known that crack curving or oscillating crack path occurs in brittle RCP in plastic and steel pipes. When the geometry of the pipe is taken into account it becomes difficult to explain the crack curving in these structures. The same oscillation has been seen in FS tests, in which the structure can be assumed to be infinitely long; thus, there is no boundary at all to reflect the stresses.

One can assume that the stress waves (emanating from the crack tip) propagate in both circumferential directions. When these two stresses waves meet at the crack tip they could deviate the crack. Nonetheless, this hypothesis is also wrong, in that the stress wave emanation from the crack tip is a continuous process, therefore it cannot periodically change the stress field at the crack tip and so cause the crack to oscillate.

Kobayashi *et al.* (1977) has studied the effect of biaxial stress ratio, σ_H/σ_z , at the crack tip and concluded that for a crack to propagate straight the biaxial stress ratio should be less than one. Later on Ramulu & Kobayashi (1985) claimed, on analysing a series of photoelastic experiments, that crack curving is associated with the stress field acting parallel to the crack tip. This observation seems relatively promising in explaining this complex event in that in order to deviate a propagating crack from its original direction, a stress parallel to the crack tip is required. The results of Knauss & Ravi-Chandar (1985) support this argument: as the width of the specimen increases the crack path becomes straight, bending of the separated beams behind the crack tip creates axial stress at the crack tip, causing the propagating crack to change its direction.

Using a laser caustic method Nishioka *et al.* (1989) have studied the mixed mode stress intensity factors, K_I and K_{II} for dynamic crack curving and subsequent crack arrest. From their studies it was concluded that in fast curving fracture, the behaviour of a dynamically propagating crack cannot be described by K_I alone. It has been also acknowledged that when a crack arrests under a mixed-mode condition, crack arrest cannot be expressed by K_{IA}

alone but the existence of K_{II} has to be taken into consideration. Therefore, a fracture parameter which includes the mixed mode effect must be used for mixed-mode crack arrest toughness.

7.3.2. Crack Branching and Bifurcation

Crack branching is a phenomenon under much attention because of its importance in comminution, drilling and rock formations. As a result of this, crack branching and bifurcation have been studied by many researchers to understand the governing mechanism(s).

Crack branching can occur in *brittle solids* such as rock, polymers and metals subjected to *low temperature* environments and/or high rates of loading.

There are three basic criteria controlling the onset of bifurcation and crack branching: distortion of crack tip stress field above a critical velocity, the critical stress intensity factor and strain energy release rate.

7.3.2.1. Critical Velocity Criterion

In dynamic fracture mechanics, high crack speed is considered to be one source of crack branching, probably as a result of Yoffe's (1951) argument on incipient branching. For a crack moving along a straight line in an infinitely two-dimensional medium under remote stresses, Yoffe presented the steady state solution for a crack of constant length. The circumferential stress exhibits two maxima in front of a crack above a velocity of about $v_c/c_I = 0.33$ where v_c and c_I are the crack and longitudinal wave speeds, respectively. It is suggested that this stress field promotes crack branching. Supporting Yoffe's criterion, Craggs (1960) argued that the critical crack velocity for bifurcation should be $v_c/c_I = 0.40$. Cotterell (1965) supported the same approach as a condition for crack branching. In contrast, from their experimental analysis, Bowden *et al.* (1967) have acknowledged that a crack may accelerate up to the time of branching, which is then accompanied by a drop of 5 to 10% in the crack velocity after branching. Using the precise ultrasonic ripple marking

technique Kerkhoff (1973) also confirmed that the crack velocity decreased by about 10% in glass immediately after branching. Ravi-Chandar & Knauss (1984 a-b), using the method of caustics in a glassy polymer, Homalite-100 specimens, observed that crack branching occurred consistently at velocities well below the ratio of $v_c/c_I = 0.25$. Anthony *et al.* (1970) reported conditions where branching in glass occurred at well below maximum speeds. The observation of these different crack velocities at the time of branching showed that a proposed critical crack velocity could not be the only prerequisite for crack branching in these materials.

7.3.2.2. Critical Stress Intensity Criterion

As it is generally observed that crack branching can occur at relatively high crack velocities, a critical stress intensity factor or a critical stress state in the region ahead of the crack tip might be the condition governing crack branching.

Clark & Irwin (1966) reported that crack branching requires a critical branching stress intensity factor. Congleton (1973) also favoured the existence of a definite branching stress intensity and derived a relationship between the stress intensity and crack branching. Kobayashi & Mall (1978) showed that crack branching consistently occurred in Homalite-100, single edge notch specimens only as K_I reached its maximum value with a negligible K_{II} . It has also been shown from this study that the branching stress intensity factor is not a unique material property.

7.3.2.3. Energy Criterion

According to this criterion crack branching occurs when the strain energy release rate exceeds the dissipated energy by a single crack. The crack might be expected to follow the path of maximum energy release, or equivalently, of minimum resistance (Sumi *et al.*, 1983, and Schindler & Sayir, 1984). Johnson & Holloway (1967) and Bansal (1977) attributed branching to the excessive supply of driving energy to the propagating crack tip. However,

Rabinovitch (1979) discussed the energy hypothesis and showed that the energy criterion equations of this model were not valid for a crack propagating at the limiting velocity.

The stress wave approach for crack branching is another area of interest. Lawn & Wilshaw (1975) and Rossmannith & Shukla (1981) suggest strong interactions between the advancing crack tip and stress waves of large amplitudes. The experimental results of Ravi-Chandar & Knauss (1984 a-b) and Kobayashi & Ramulu (1981) indicated crack branching without stress wave interaction, thus it may be concluded that the stress wave approaches for crack branching is a conceivable cause but does not seem the only necessary condition for crack branching.

Available experimental data on crack branching varies from material to material and appears to be a function of the test method. In addition, the conditions in successful macroscopic branching are geometry dependent, as are the branching angles. In the DCB specimen, for instance, due to the loading condition large bending stresses can cause the crack to deviate substantially and the branch attempt is less likely to fail, having reached some minimum length.

A static FE analysis of Wilson & Cherepko (1983) showed that after branching the effective stress intensity for each branch was less than the unbranched value but the total G exceeded the unbranched level which is consistent with energy based branching theories.

From the experimental and theoretical analysis, it was recognised that one single criterion is insufficient to explain branching. For that reason, some authors have postulated more than one canon for successful branching. Kobayashi (1984) has favoured a critical stress intensity and a critical distance criterion, *i.e.* a critical stress at a critical distance, for successful branching. Rosenfield & Kanninen (1973) suggested a critical stress intensity and a critical crack velocity for crack branching.

More recently, Takayashi & Arakawa (1990) pointed out that the density of fracture surface markings is not uniquely determined by $\hat{d}$ or R^* (the product of crack resistance), and no

definite critical values could be determined for $\dot{a}$ or R^* at the onset of crack branching, but crack branching takes place when the following relation is reached:

$$R^* \dot{a} = C_B \quad (7.3)$$

where, C_B is considered to be a material constant.

7.3.3. Micromechanical Approach to Crack Curving and Branching

It has long been known that all materials, especially brittle ones, contain micro-cracks coming from the manufacturing process in the form of inclusions, inhomogeneties, the interface between dissimilar constituents, inclusions and voids or any other possible material or geometric discontinuities in different size and directions (Jayatilaka & Trustrum, 1977; Jayatilaka & Nanayakkara, 1981; Horri & Nemat-Nasser, 1986). Therefore variable sizes, shapes and orientation (with respect to the applied load) of flaws in a material can account for scatter in the behaviour, unlike the materials where plastic deformation takes place.

It is proved experimentally that, at the tip of a running crack, a tuft of micro-cracks always exists (Ravi-Chandar & Knauss, 1984 a-b; Theocaris, 1988) and as the crack tip approaches these micro cracks, they are forced to grow as a result of crack tip stress field. Further crack growth results in the coalescence of the micro-cracks in the crack region and reduces the stress intensity factor by the increase in the compliance of the crack tip core region (Ramulu & Kobayashi, 1985). At the same time, the remote stress component parallel to the crack-tip, σ_z , increases in magnitude, Rice (1974). This increasing stress parallel to the crack activates macro-cracks away from the crack tip-plane. If the micro-cracks are sufficiently close to the main crack tip they will tend to divert the crack away from its original plane. It has also been observed that a single crack tip diversion results in crack curving but multiple crack tip diversion results in crack branching. If the applied stress intensity factor K_I is high enough, branched cracks will continue to propagate and result in successful crack branches (Congleton, 1973).

Based on this microscopic crack model Theocaris *et al* (1989) proposed a twin-crack model in which it is assumed that only two equal microcracks, symmetrically placed to the main crack axis, are responsible for the position and angle of macroscopic crack branching and crack curving. The two microcracks may either progressively deviate from each other or coalesce, depending upon the geometry of the crack zone and crack velocity with associated applied stress. Thus, it was acknowledged that definite branching points do not exist, and that the crack is in the state of microscopic branching from the early steps of its propagation. Consequently, it was reported that a single critical quantity necessary for branching to occur is unrealistic. Instead, they proposed two groups of parameters governing crack branching. The first group includes the applied stress and the crack velocity which are definite macroscopic quantities and was named the 'deterministic character' of the phenomenon. The second group covers the micro-crack parameters, namely the ratio of microcracks to the main crack as well as the inclination of the microscopic crack with respect to the main crack at the crack tip. This group was named 'stochastic character'.

7.3.4. Crack Branching and Curving in Pipes

7.3.4.1. General Background

One of the distinct characteristics of RCP in pressurized PE pipe is a 'sinusoidal' path. Similar crack paths were also reported by Duffy *et al* (1969) in pressurized steel pipe when the crack speed was relatively fast, between 350 and 760 ms⁻¹, and fracture occurred by cleavage. On the contrary, when the fracture occurred by shear type and crack speed was relatively slow, typically between 75 and 250 ms⁻¹, fractures generally resulted in a single crack travelling in a straight line. For an oscillating crack path, the fracture surface was reported to be very brittle and crystalline in appearance, but the shear type of fracture exhibited considerable distortion and thinning along the straight crack path.

Shannon & Wells (1974) studied brittle crack propagation in pressurised thin walled PVC pipe, having 160 mm outside diameter and 3.3 mm wall thickness, by means of laboratory

tests. Crack speed measurements revealed an average crack speed of 340 ms^{-1} . In these experiments they also studied the variation of brittle crack speed on the inner and outer surfaces of pipes along the sinusoidal crack path. From these measurements it was acknowledged that the crack is plane fronted whilst travelling in the spiral direction but that at all other points the tip of the crack leads on the inner surface. It was also pointed out that radial expansion of the fractured pipe caused bending stresses to be superimposed on the stress field ahead of the crack tip, producing a through thickness stress gradient with a maximum value on the inner surface. This stress was reported to be responsible for the crack leading on the inner surface and for this effect being most clear when the crack was travelling in the axial direction.

7.3.4.2. Features of Crack Branching and Curving in PE pipes

The present experimental results have revealed that there is no unique shape for either crack path shape or branching. For a given pipe dimension, pressure, and temperature almost identical crack path shapes were obtained. This could be seen in Fig. 7.11 showing more or less the same crack path trajectories on the BP 002-40 and Aldyl 'A' pipes tested under the same pressure and temperature. However, the parameters affecting RCP performance have a recognisable influence on crack path trajectories. One of the most characteristic features of crack paths observed during the present study is that in all the tests the initiated crack propagates for about 20 cm straight in the axial direction and then either starts to deviate from its original direction or propagates straight along the whole test length. One possible explanation of this initial straight crack propagation could be the fact that as the projectile impacts the pipe it acts as a wedge. Thus the initial stage of crack propagation is due to the wedge which creates such a large dynamic stress field around the impact zone that it almost always results in an initial fast crack: up to 700 ms^{-1} . Thus, the pressure in the pipe or that acting on the flaps behind the crack tip hardly has any influence on either crack initiation or the initial stage of crack propagation.

For the relation between crack branching and off-axis crack propagation, it has to be acknowledged that the possibility of crack branching increases with the increasing magnitude of crack oscillation . By extending the interaction further it could be pointed out that no successful crack branching occurred for a straight propagating crack. Furthermore, it became well understood that any parameter reducing the magnitude of crack curving tended to minimise crack branching. Broken PE pipe specimens on which branching and crack curving were seen together, showed a general trend that when crack branching took place, whether it was successful or just an attempt, it occurred just before the peak of the oscillating crack path, as schematically shown in Fig. 7.12.

At lower pressures branching attempts usually fail, the main crack continuing through a sheaf of 2 - 30 cm long arrested sub-branches. At intermediate pressures, both branches of the bifurcated cracks may continue to propagate alongside each other, although the trajectory of each branch does not always follow each other symmetrically as shown in Fig. 7.13. In some cases, after some propagation distance, the paths re-join to cut off and eject an 'island'.

The parameters that are observed effecting the crack trajectories are pressure, pipe thickness, temperature, and pipe grade (*i.e.*, toughness). Their influence on crack path instability is discussed below.

i) **pressure effect:** for given test conditions, increasing the test pressure tends to decrease the magnitude of oscillation as shown in Fig. 7.14. This might be due the fact that the strain energy (as well as the circumferential stress) increases with test pressure. As a result, the contribution of the crack driving force coming from the pressure acting on the flaps behind the crack tip decreases and the driving force is mainly governed by the strain energy stored in the pipe wall as the test pressure is increased. Thus, any parameter diminishing the effect of pressure behind the crack tip tends to result in smoother crack paths and fewer crack branching attempts. There are two other experimental observations supporting the validity of this observation:

these are the cage clearance and internal gas volume effects. For the former, it was observed that the larger the clearance between the pipe and the containment cage the higher the oscillation. For the latter, tests with reduced volume (see Chapter 6) revealed that the crack propagated in a straight manner when the gas volume was reduced.

ii) thickness effect: Post-mortem examination of the crack trajectories revealed that increasing the thickness tends to increase the magnitude of crack oscillation. The effect was observed in two different test cases. Firstly, 180 mm SDR 11 and 17.6 BP 002-40 pipes tested under the same test circumstances revealed that increasing SDR magnifies both the wave-length and amplitude of crack oscillation. Secondly, the thickness of an 180 mm SDR BP 002-50 pipe was decreased step by step from 17.2 to 4.5 mm and tested at 0 °C at the pressure of 3.5 bar. Fig. 7.15 shows the diminishing magnitude of crack curving with the decreased thickness.

iii) temperature effect: The effect of this parameter is the most striking one. The lower the temperature, the larger the oscillation and attempts for crack branching.

iv) pipe grade (material) effect: It is clearly evidenced that materials with higher fracture toughnesses show fewer off-axis crack paths and attempts for crack branching. Additionally, it became apparent that materials having the same critical pressure for any test temperature tend to exhibit the same crack path morphology.

v) slitting effect: Introducing a fine slit on the inner surface along the pipe axis eliminated the typical feature of RCP: crack oscillation. In all the tests when the crack followed the slit, crack followed a straight path with no deviation. Despite some observed branching attempt, particularly at the outer side of the surface, no successful crack branching took place. It is envisaged

that the main reason for such a straight crack propagation is the fact that crack velocity in slit pipe experiments is relatively higher, compared to the tests on unslit tests, thus there is not enough time for pressure behind the crack tip to act on the flaps which is the source of high axial stresses around the crack tip, If there is, $\sigma_H/\sigma_Z \gg 1$ at the crack tip thus, the crack propagation is governed and controlled by the circumferential stresses.

7.3.5. Crack Curving and Branching Mechanism in PE Gas Pipes.

The microscopic aspects of crack curving and branching could be explained by Ramulu *et al.* (1983)'s model in which they pointed out that micro-cracks nucleate from inclusions and voids in the vicinity of the crack tip and grow as a result of the imposed crack-tip stress field, Fig. 7.16. They continued that a further crack growth results in coalescence of the micro-cracks in the crack-tip region and reduces the stress intensity factor due to the increased compliance of the crack tip. The significance of the stress component acting parallel to the crack was pointed out: it activates microcracks away from the crack tip plane. It is also pointed out that a single crack tip diversion results in crack curving and multiple crack diversion causes crack branching.

While a fast crack is propagating in the material, the region in front of the crack tip is stretched in the axial direction and elongates which aligns the microcracks and voids in this direction as shown in Fig. 7.16. As a result, it is most likely that when the main crack tip reaches these aligned cracks it will branch into two. Since higher pressures and/or higher crack velocities reduce the magnitude of the axial stress (inertial effect) the crack propagation process is governed mostly by circumferential stresses at the crack tip and thus, under such circumstances the propagating crack follows a straight crack path.

Although this model could explain the nature of crack curving and crack branching phenomena in PE gas pipe, there are some questions to be answered. Why does a propagating crack not follow a random pattern (since the microcracks in the material are arbitrary distributed) but a regular 'sine-like' crack path whose amplitude and wave length are

temperature, pressure, thickness etc. dependent ? Why does a faster propagating crack tend to follow a less wavy path? For some materials (particularly tougher PEs), or for any PE at higher temperatures, why does a propagating crack follow a straight path? (The number of these questions could be increased further!).

The answer to the above questions is rooted in the fact that, due to the deformation of the pipe, a high axial stress state is created. It is the ratio of this stress to the circumferential stress that controls crack branching and oscillation. As the quasi-static and dynamic FE analysis indicated, the ratio could be as high as 3. It is believed that the creation of such a stress field at the crack tip takes some time (or crack propagation length) but this field does not change very fast while the crack oscillates. Bearing this in mind, it could be argued that at the crack tip the axial stress state is not symmetrical but varies with the position of crack tip on the oscillating crack path and takes the forms shown in Fig. 7.17. When the crack propagates in a straight manner, the axial stress state at the crack tip is symmetrical (Fig. 7.17-a). At this point, crack curving depends upon the ratio of σ_H to σ_Z , which is controlled by crack velocity and the rate of pressure decay behind the crack tip. When $\sigma_H/\sigma_Z \leq 1$ (because of its symmetry), crack propagation is dominated by σ_Z thus, the crack could either deviate clockwise or anti-clockwise, or both clockwise and anti-clockwise (branches). As the crack propagates on a curved crack path the value of σ_H/σ_Z increases and loses its axial stress symmetry (Fig. 7.17-b). Both the axial and circumferential stresses at the crack tip could be combined to find out the resultant stress as shown in Fig. 7.18. It is well known that a crack will tend to cross the direction of maximum global principal stresses. The magnitude and direction of the maximum principal stresses for straight and deviated cracks are plotted in Figs. 8.11.

As far as the effect of test temperature on RCP patterns is concerned, increasing test temperature and/or decreasing the wall thickness reduces the oscillation as well as branching. The dependence of crack branching and crack path on these parameters might be explained by the size of the plastic zone at the tip of a propagating crack. Decreasing the temperature as well as increasing the specimen thickness reduces the plastic zone size. A larger plastic zone

at the crack tip minimises not only the effect of stress parallel to the crack tip but also the role of microcracks in front of the crack tip. As a result this induces the crack to follow a straight path.

Shannon & Wells (1974) studied the variation of crack velocity measured for PVC gas pressurised pipe on the inner and outer surfaces along the oscillating crack paths. Their results are depicted in Fig. 7.19 showing the fact that although the crack velocity at the inner surface remains more or less constant but at the outer surface it attains a maximum value of about 1400 ms^{-1} . In the present technique the variation of crack velocity on the outer pipe surface along the oscillating crack path was measured using 20 timing wires painted on the outer surface of the pipe. The distance between the each timing wire was 10 mm. Fig. 7.20 shows the crack velocity history along the oscillating crack path indicating the fact that the crack velocity takes the maximum value around the peak of the oscillation and then starts to slow down.

7.3.6. Experiments on PMMA Pipe (Crack Curving)

In order to simulate the effect of pressure acting on the flaps behind the crack tip a series of experiments were conducted on 50 mm diameter PMMA pipe specimens with a 3 mm thickness. Initially, thin axial slots of different lengths were cut at one end of the PMMA pipes. The reasons for the selection of this material for this particular tests were due to the fact that the material is, transparent enough for crack tip propagation observations and brittle enough for a crack to propagate even at very low crack velocities.

Fig. 7.21-a shows the specimens with the initial notches in them and loading conditions used in those experiments. For these experiments the initial crack length and the axial distance between initial crack tip and load application point were changed systematically.

The experiments were carried out using an Instron machine at a cross-head speed of 20 mm/min. Fig. 7.21-a shows the observed crack paths for two different loading conditions. In all tests the crack started to propagate in the axial direction but sooner or later deviated

towards the circumferential direction. It was observed that if the initial axial distance between the crack tip and load application point was relatively short the crack propagated axially up to a certain length and then deviated to circumferential direction. In contrast, if the initial distance was increased sufficiently, the crack propagated by deviating immediately towards the circumferential direction. From these tests it could be concluded that the differences in the crack paths were entirely due to the stress field at the crack tip or, alternatively, the ratio of the axial stress to the circumferential stress at the crack tip created by loading system dominated the crack trajectory. It is also worth mentioning that as the crack started to deviate from its axial direction to the circumferential its velocity increased significantly and also the recorded force-displacement records have revealed that there was a sharp decrease in force as the crack started to diverge. In all these tests it was observed that the crack did not oscillate but deviated and in some cases returned towards the initiation section, as shown in Fig. 7.21a.

In order to keep the stress state at the crack tip unchanged (*i.e.* independent from crack propagation length as in actual pipe case), another series of tests were carried out on the PMMA pipe using the test rig shown in Fig. 7.21-b. It was intended that with this specially designed rig the conical part could simulate the pressure acting on the flaps behind the crack tip and the 'O' ring could simulate the internal pressure (or the circumferential stress) around the crack tip region. By adjusting the nut the stress value could be changed. The general conclusion from these tests is that as a force was applied in the direction shown in the figure the crack started to curve and subsequently, followed a spiral trajectory as shown in Fig. 7.21-b.

From these tests it is now clear that the crack path instability taking place in RCP in PE pipes is not due to stress wave interaction during the crack propagation but to the enormous axial stresses at the crack tip, created by the pressure acting on the flaps behind the crack tip. The second important fact is that as the crack deviates from its initial axial to circumferential direction the crack velocity increases, thus at the initial stage of crack deviations the crack speed accelerates but as it propagates further its velocity drops.

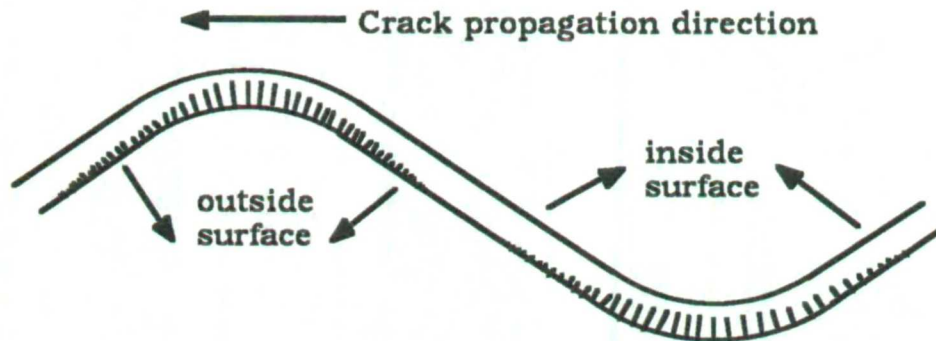


Figure 7.1 Schematic presentation of the size and location of undercutting seen on fracture surface of sinusoidal-like crack path.

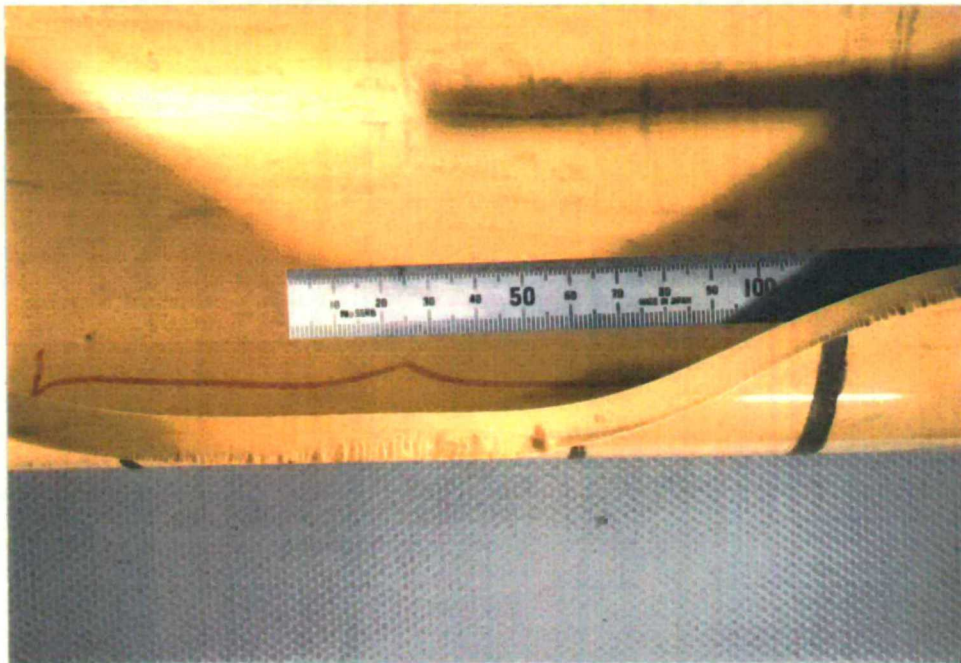


Figure 7.2 Fracture surface of broken pipe along oscillating crack path in Du Pont's Aldyl 'A' 180 SDR 17.6 pipe tested at -15°C and 4.5 bar.

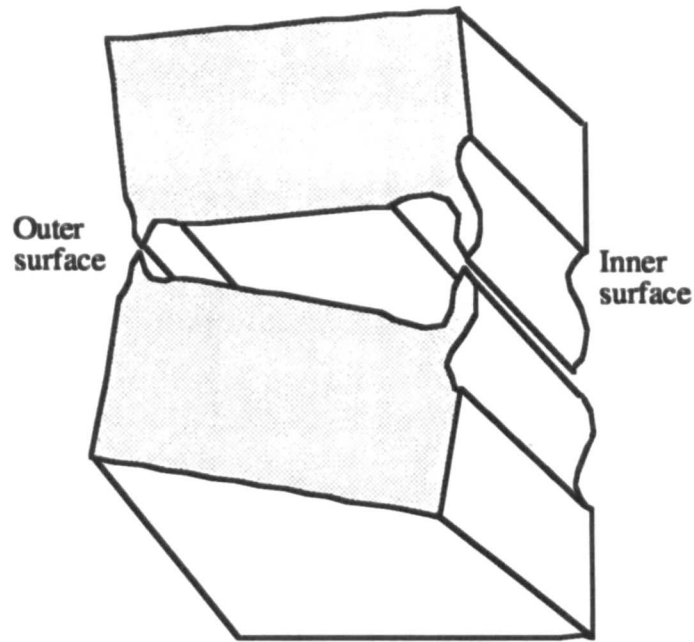


Figure 7.3 Schematic view of broken pipe surfaces showing the size and shape of the tear-lips at both inner and outer surface.

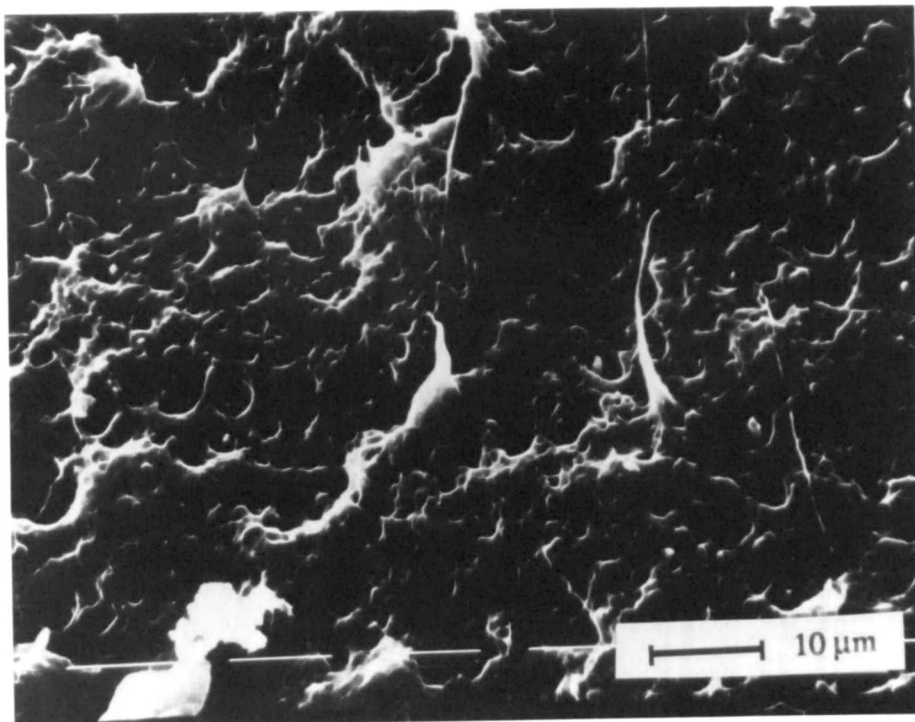


Figure 7.4 Magnified view of the fracture surface showing a number of small dimples and in the centre of them inclusion like particles (180 SDR 11 TUB 125 tested at a temperature of -15 °C and pressure of 12 bar).

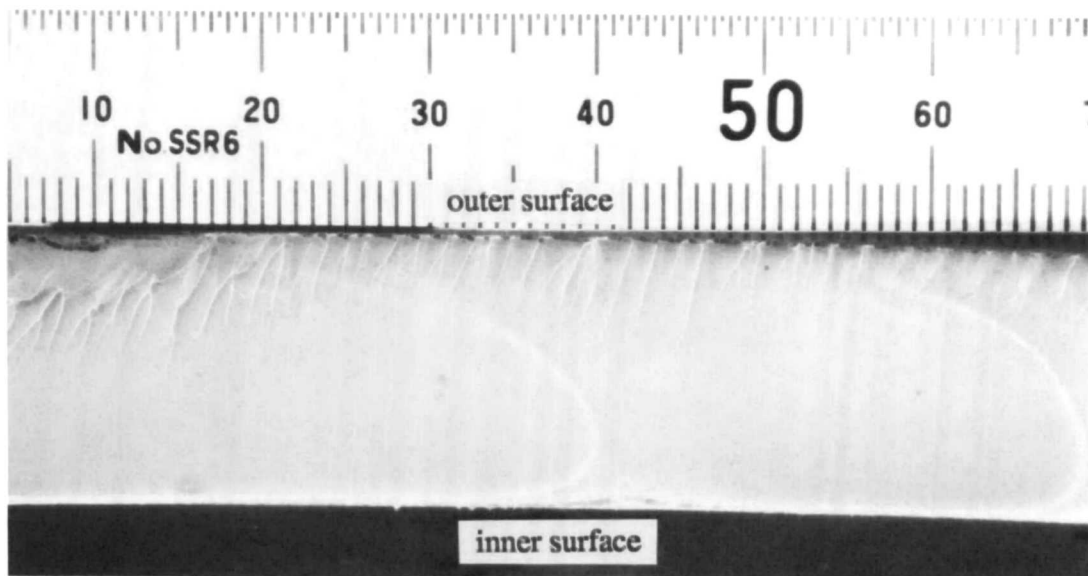


Figure 7.5 Parabolic markings at the fracture surface showing the crack front shape as well as crack arrest attempts during RCP, TUB 125 180 SDR 11, tested at -30 °C and 3.2 bar.

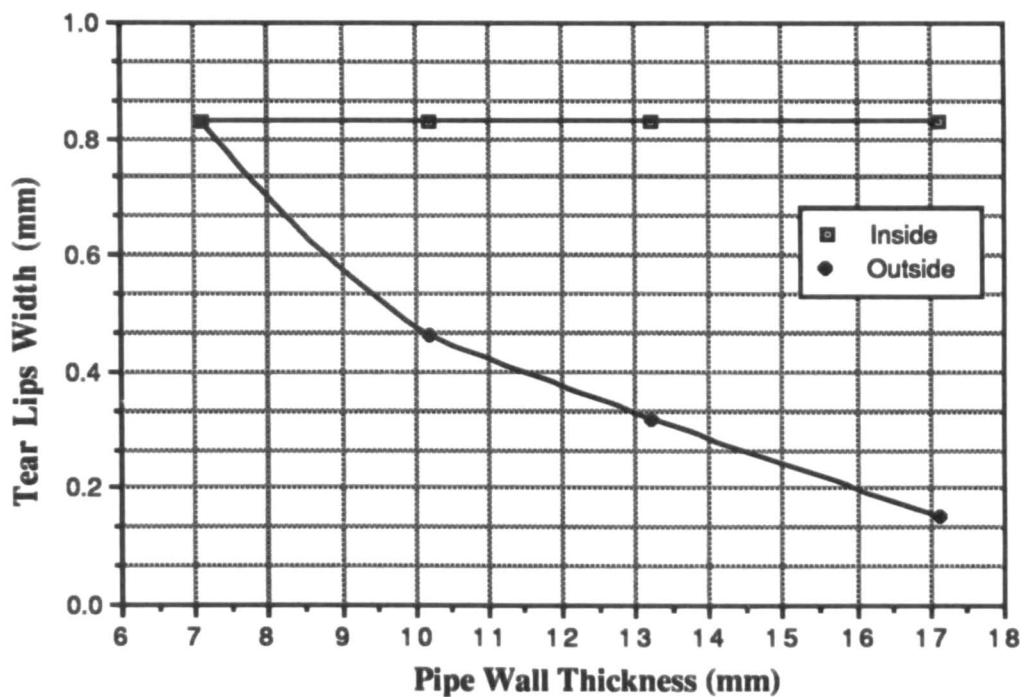


Figure 7.6 The size of tear lips at both inner and outer surfaces of BP 002-50 180 mm pipe, tested at 0°C and 3.5 bar, with different thicknesses, showing constant tear-lips size at the inner section but decreasing tear-lip size with increasing pipe thickness at the outer section.

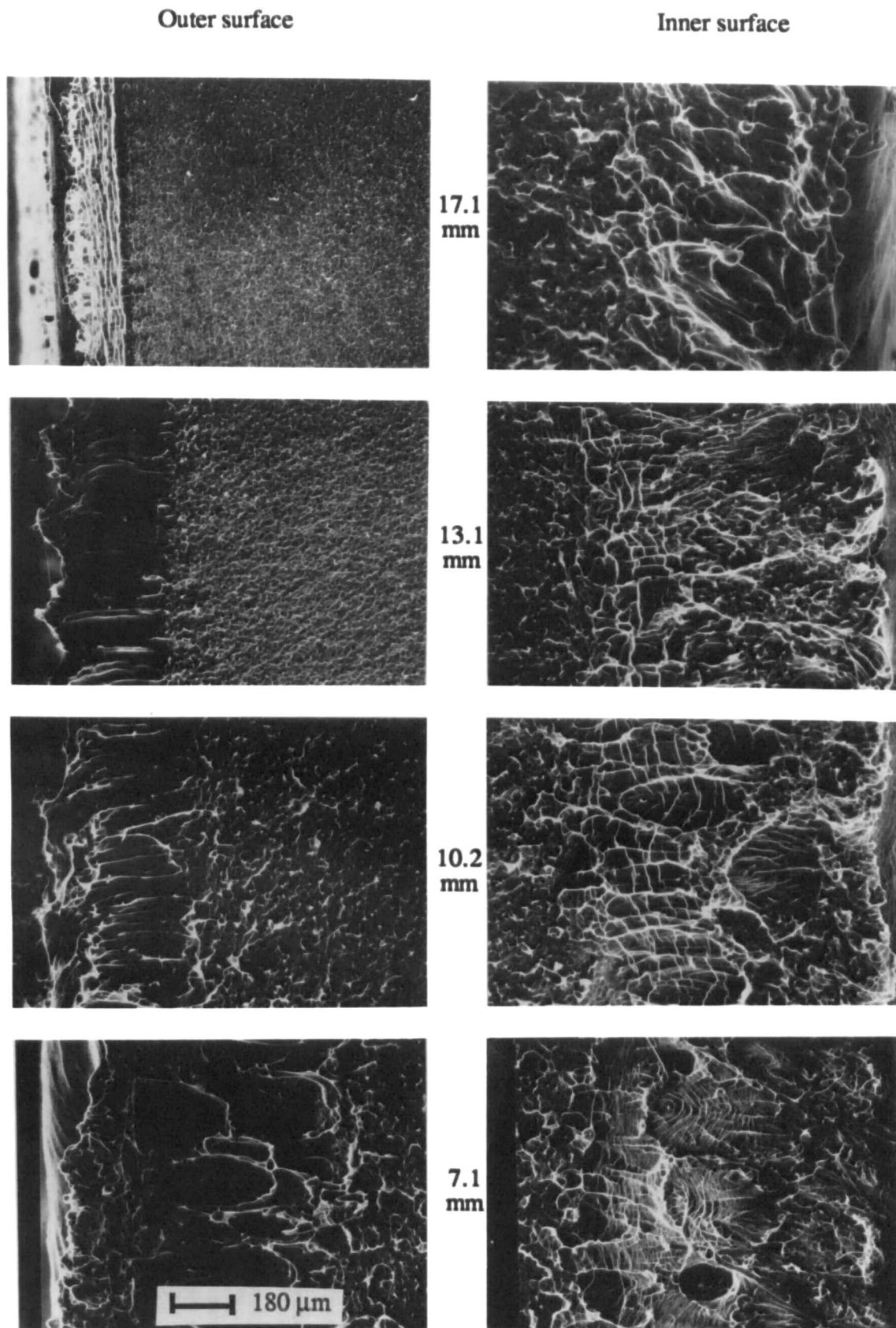


Figure 7.7 Variation of fracture surface on both inside and outside sections with different pipe thicknesses for BP 002-40 180 mm step-tapered pipe, tested at 0 °C and 3.5 bar.

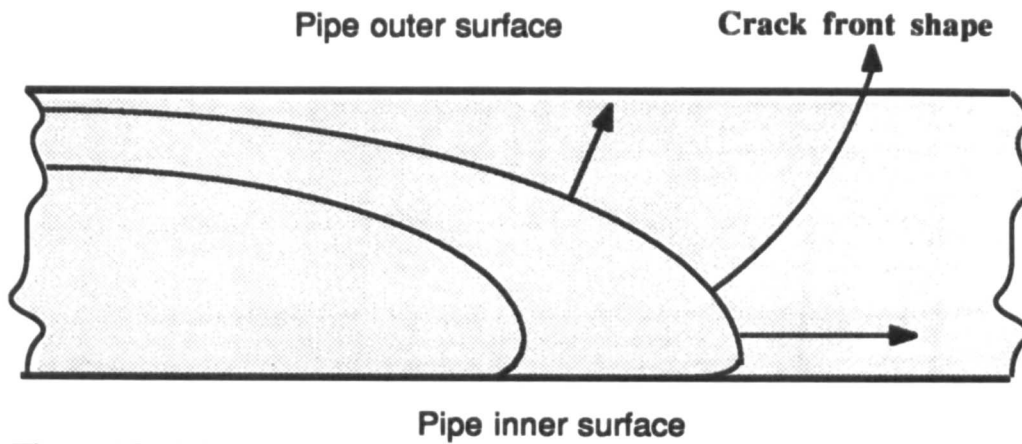
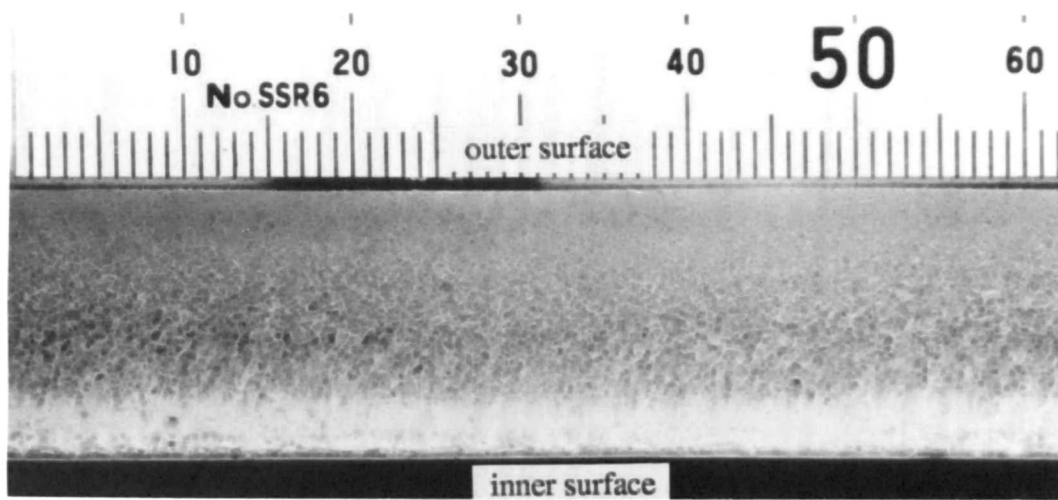
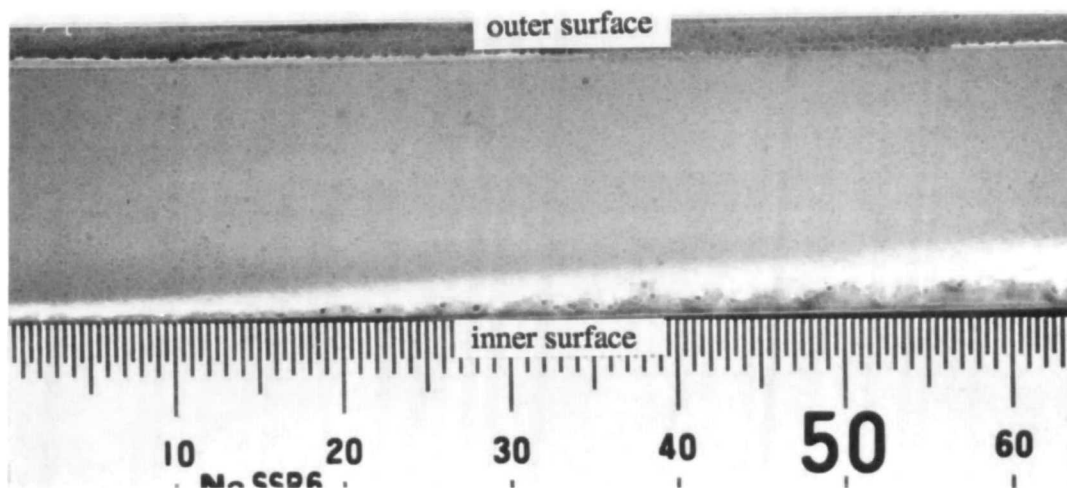


Figure 7.8 Schematic representation of crack front shape of RCP in PE pipes.



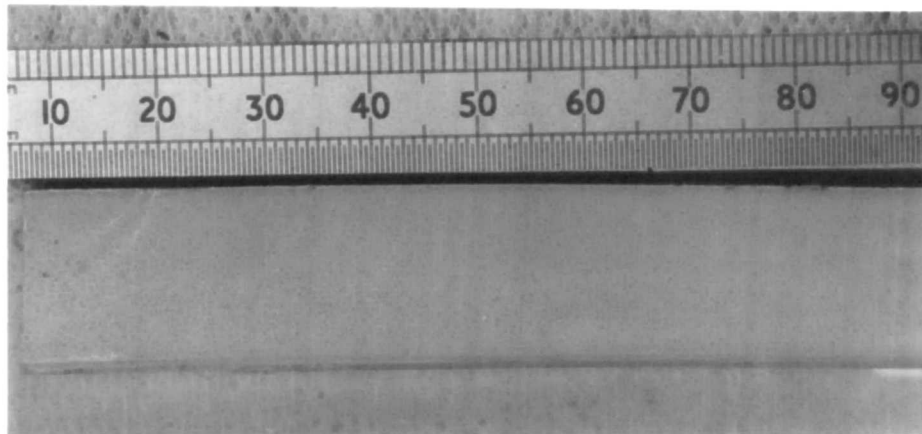
(a)



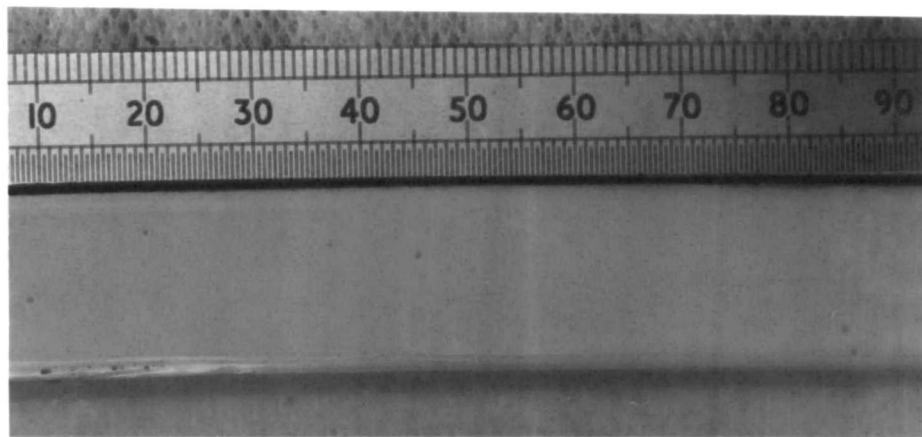
(b)

Figure 7.9 Fracture surface of 180 mm SDR 11 PE pipes tested at 13 bar showing that although the p_{CS4} values are similar to each other, fracture surfaces differ

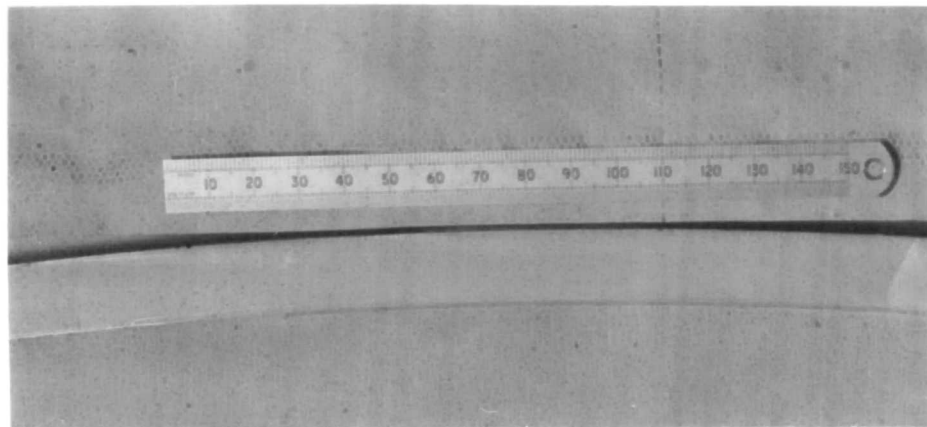
a) TUB 125 tested at -15 °C, b) BP 002-40 tested at 15 °C.



(a)



(b)



(c)

Fig 7.10 Effect of inner surface slitting upon: 180 SDR 11, tested at 1.5 bar (top is outer, and bottom is inner surface)

a) fracture surface morphology for TUB 125 tested at 20 °C, **b)** fracture surface morphology, BP 002-40 tested at 20 °C, and **c)** the crack front shape, TUB 125 tested at -15 °C.

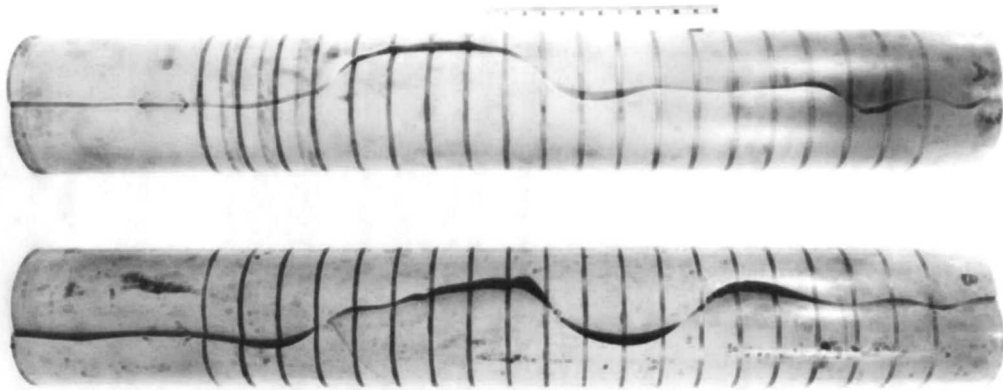


Figure 7.11 RCP propagation along 180 mm SDR 17.6 two MDPE pipes (BP 002-40 and Aldyl 'A') at a pressure of 2.2 bar tested at -15 °C, showing that the paths of propagating cracks are similar to each other and crack branching occurred almost at the same propagation distances from the initiation point.

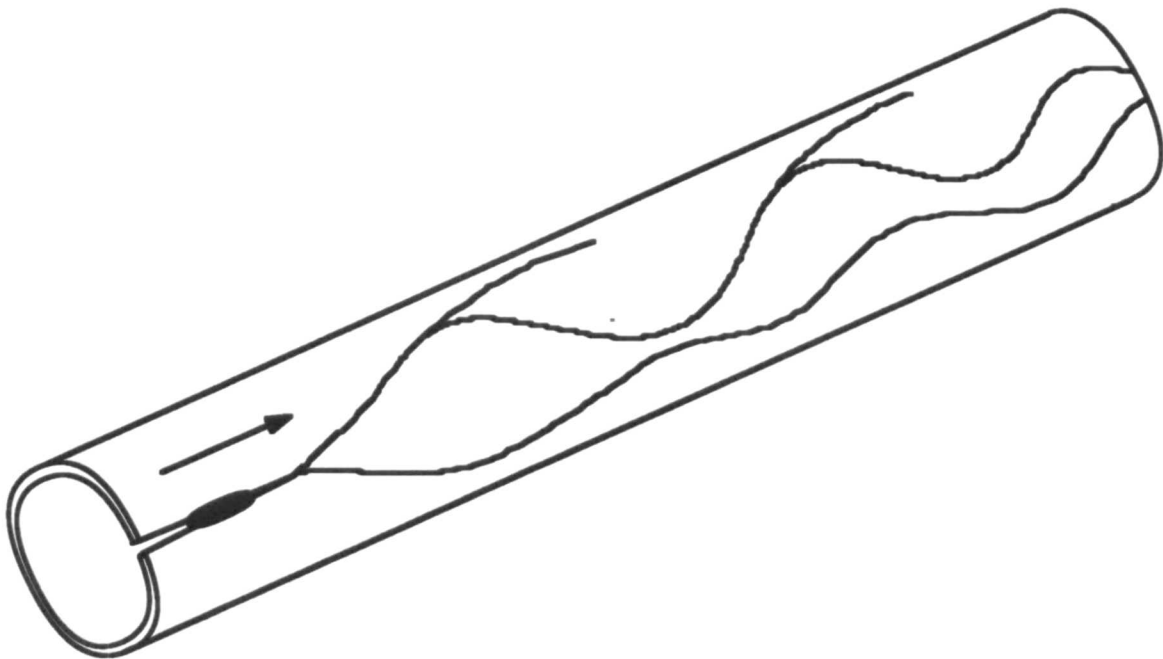
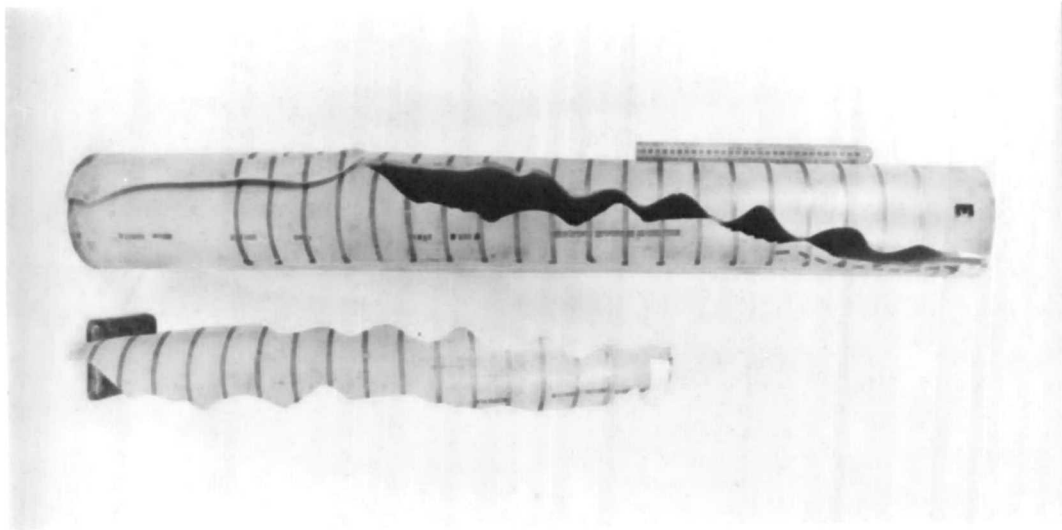
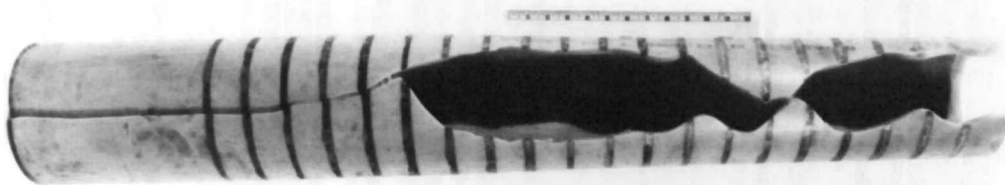


Figure 7.12 Schematic illustration of crack branching and curving of RCP in gas pressurised PE pipes.



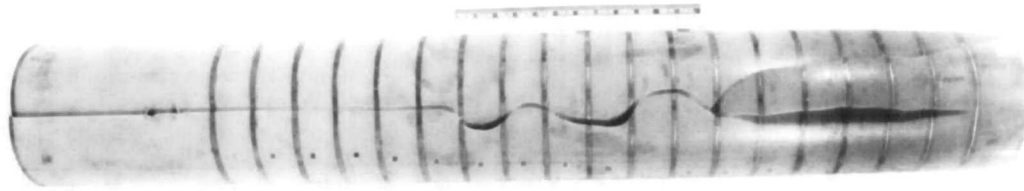
(a)



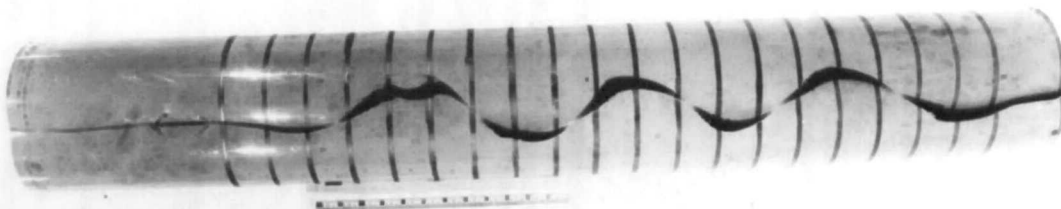
(b)

Figure 7.13 Crack paths of a branched crack showing the unsymmetric crack trajectories along the pipe of 180 BP 002-40 180 mm SDR 17.6 pipe for the temperature of $-15\text{ }^{\circ}\text{C}$ and pressure of;

a) 7.5 bar
b) 4.0 bar .



(a)



(b)

Figure 7.14 Effect of the test pressure on crack propagation trajectories for BP 002-40 180 SDR 17.6 pipe, tested at -15°C
a) 16.5 bar test pressure
b) 2.5 bar test pressure.

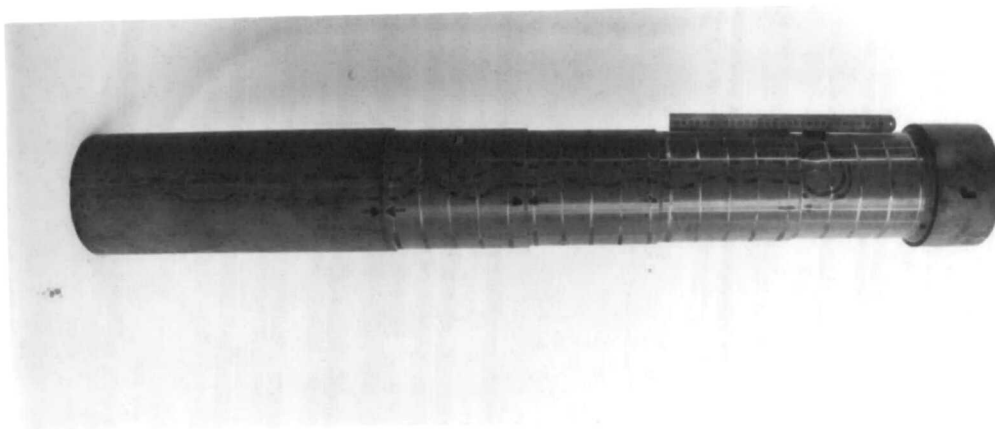


Figure 7.15 Effect of pipe thickness on crack path for BP 002-50 180 SDR 11 tested at 0°C and 3.5 bar, showing smoother oscillation with decreased thickness.

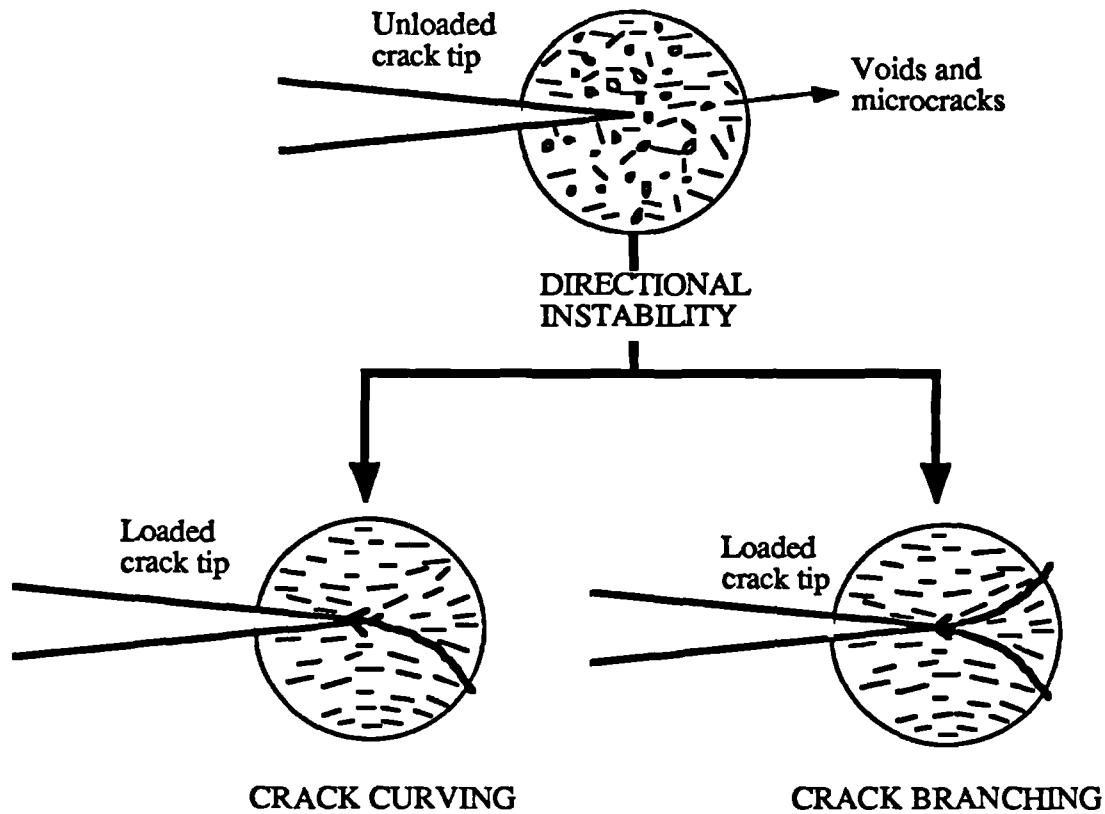


Figure 7.16 Macroscopic and microscopic explanation of crack curving and branching phenomenon (Ramulu *et al.*, 1983).

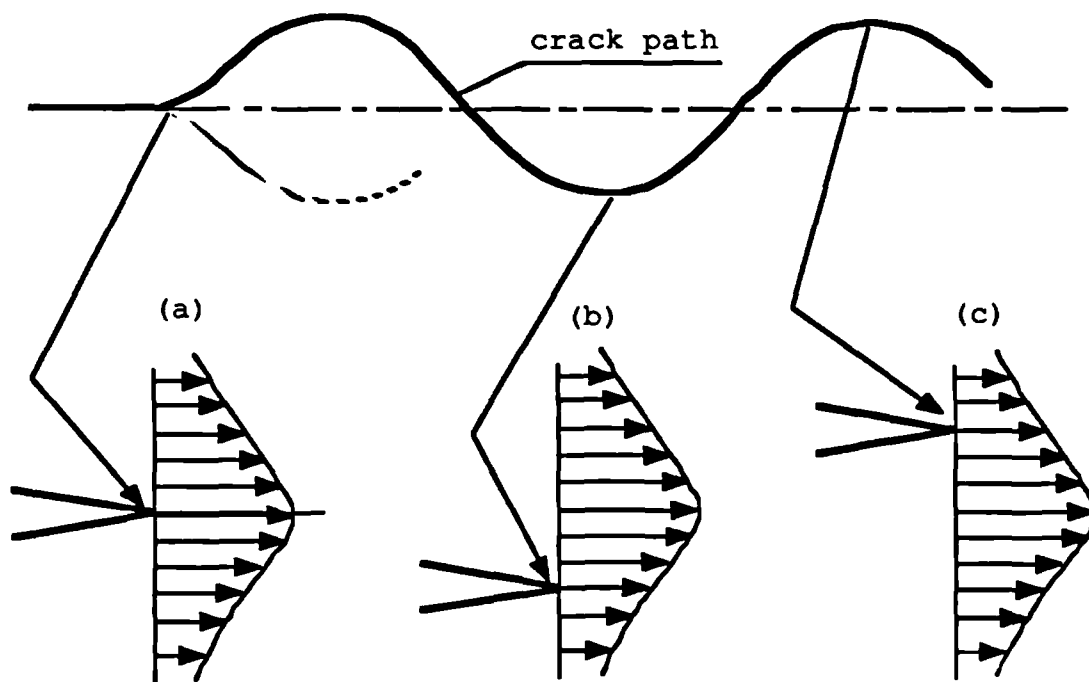


Figure 7.17 Variation of the axial stress field in front of the crack tip along the oscillating crack path.

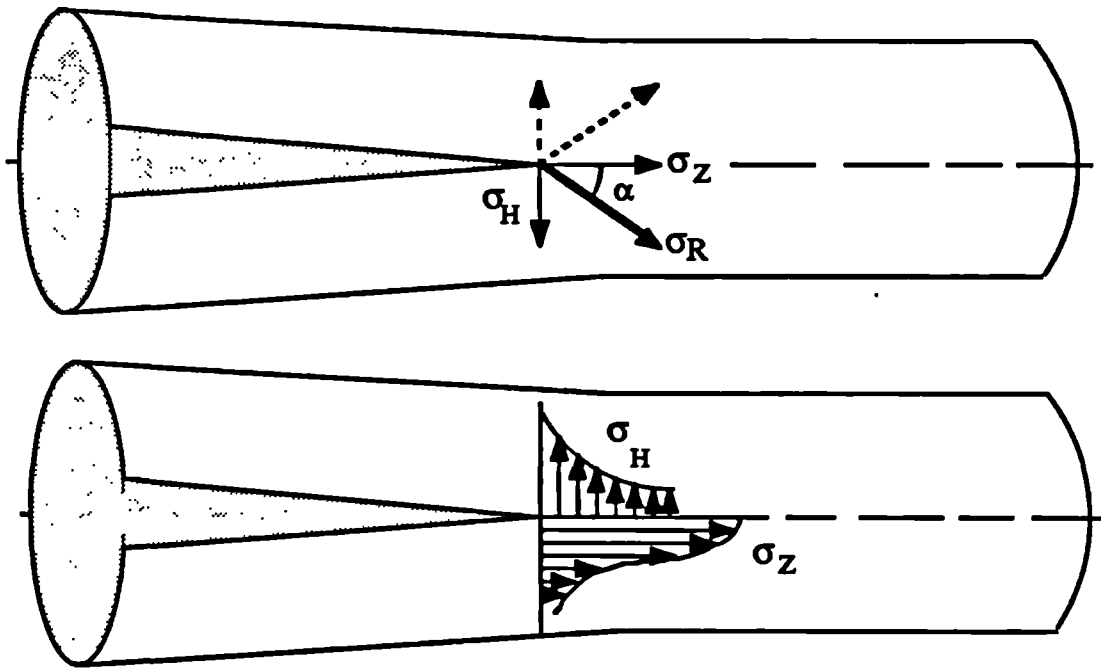


Figure 7.18 Axial and circumferential as well as resultant stresses in front of a propagating crack.

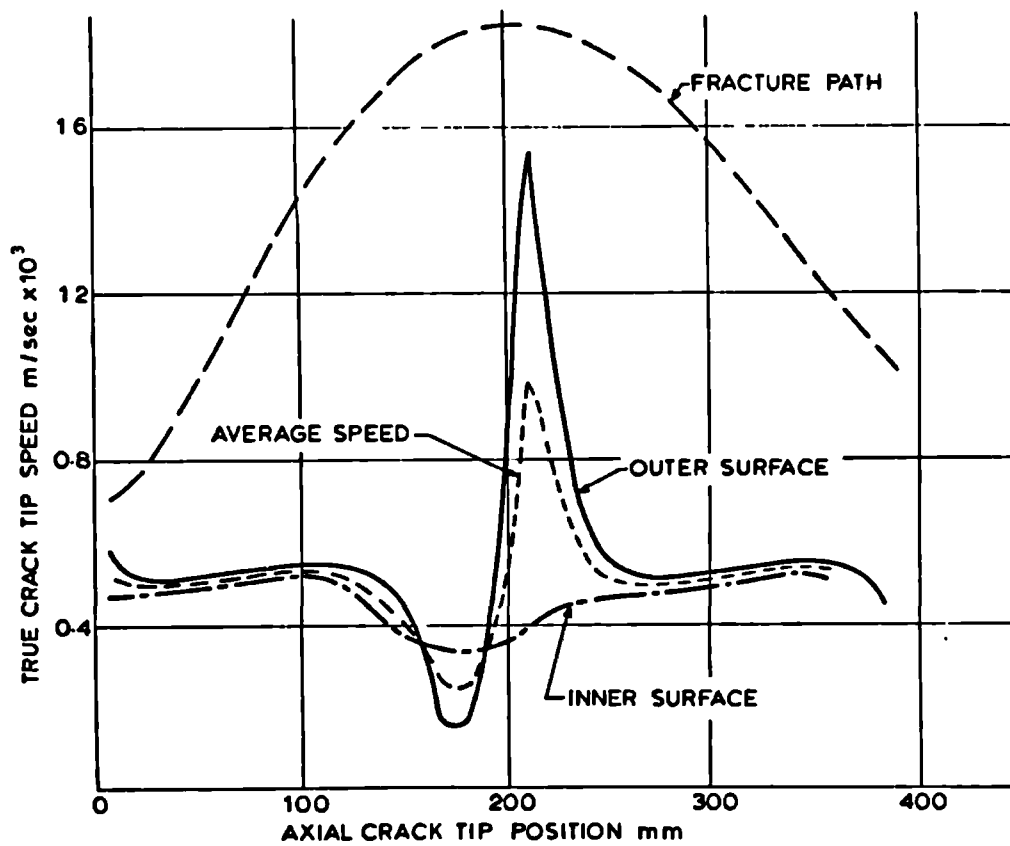


Figure 7.19 Variation of crack tip velocity on the inner and outer surface of a fracturing PVC pipe (Shannon & Wells, 1974).

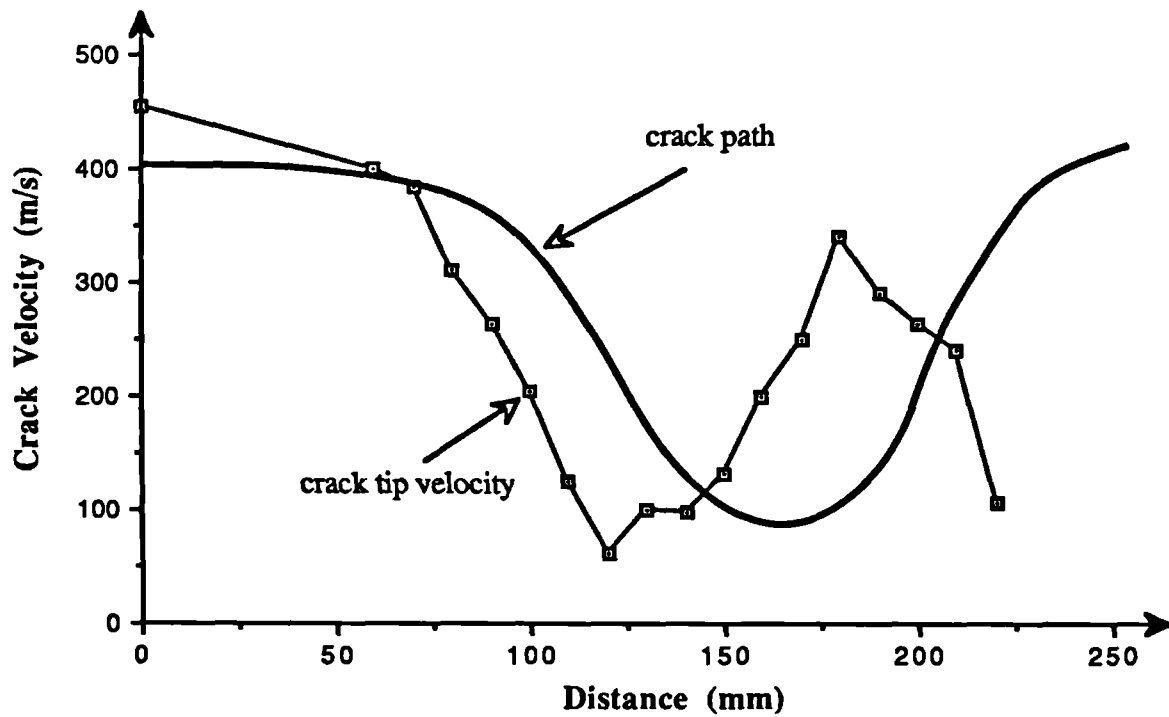


Figure 7.20 Variation of crack tip velocity along the oscillating RCP in MDPE 180 SDR 17.6 pipe tested at -15°C .

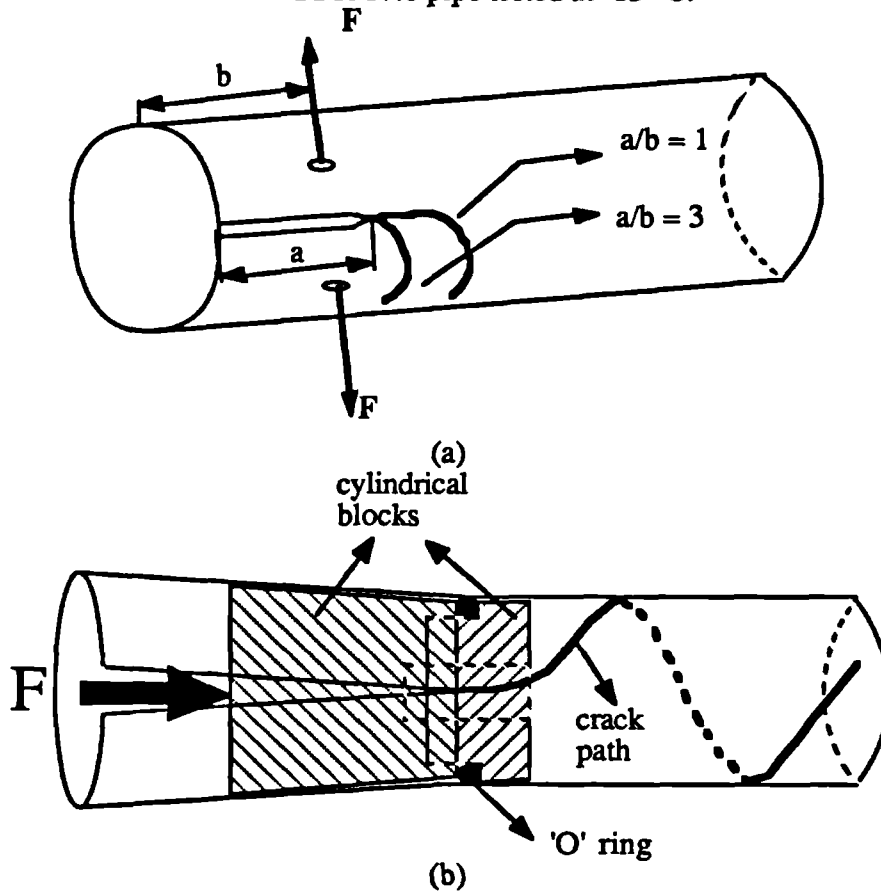


Figure 7.21 Schematic presentation of loading conditions and corresponding observed crack paths obtained from quasi-statically loaded PMMA pipes.

CHAPTER 8

ANALYTICAL AND NUMERICAL ANALYSIS

8.1. Introduction

This chapter considers the modelling of RCP in gas pressurized pipes, which is very complex in nature due to the interaction between inertial effects, three dimensional gas dynamics, visco-elastic response of the pipe material and deformation of the pipe flaps behind the crack tip. To highlight this problem a simple cantilever beam model is used to calculate the magnitude of the axial stresses around the crack tip. To gain more information about the stresses around the crack tip under quasi-static loading conditions, an FE code was used, and the results are discussed for a particular pressure and dimensions. Finally, the results from a fully dynamic special FE code are presented and discussed.

8.2. Cantilever Beam Model

The knowledge of both the axial and circumferential stresses at the crack tip has a significant importance in understanding the nature of crack propagation, and especially the oscillating crack path which is the main feature of RCP in PE pipes. To model this, the section of the pipe behind crack tip is modelled as a cantilever beam encastred at the crack tip. The other end of the pipe is kept free and pressure acts on the upper surface of the pipe having an inside diameter, D_i , as shown in Figs. 8.1 and 8.2. The pressure distribution on the cantilever beam has to be known to carry out the analysis and this can be written as $p = p(x)$.

The centroid of a semicircular ring having a thickness t , as shown in Fig. 8.2a, can be calculated as;

$$\bar{y} = \frac{\int y \, da}{\int da} \quad (8.1)$$

From which it can be shown that,

$$\bar{Y} = \frac{2}{3\pi} \left(\frac{D_o^3 - D_i^3}{D_o^2 - D_i^2} \right) \quad (8.2)$$

for the sake of simplicity this can be approximated as;

$$\bar{Y} = \frac{2}{\pi} R_{avg} \quad (8.3)$$

The centroid of the area given by the $p = p(x)$ curve is required in the calculations and can be calculated as:

$$\bar{X} = \int_0^l \frac{x p(x) dx}{A} \quad (8.4)$$

where, x is the axial distance between the crack tip and any section behind the crack tip, Fig. 8.1. A denotes the area under the $p(x)$ curve behind the crack tip, as shown in Fig. 8.2b, and may be written as:

$$A = \int_0^l p(x) dx \quad (8.5)$$

The second moment of area of a semicircular ring having a thickness t is simply given by,

$$I = 0.095 \pi t R_{avg}^3 \quad (8.6)$$

where t is the pipe thickness, and R_{avg} is the mean radius of the pipe. As the pressure acts on the split pipe surface it will create a maximum axial stress at its encastred end and this can be calculated by:

$$\sigma_z = \frac{M}{I} \bar{Y} = \frac{F \bar{X} \bar{Y}}{I} \quad (8.7)$$

where F is the total force acting on the surface, hence,

$$F = \int_0^l D_i p(x) dx \quad (8.8)$$

From above equations, an approximate value of the maximum axial stress at the crack tip may be found.

To check the accuracy of the model the pressure history behind the crack shown in Fig. 8.3 is taken into account in the above calculations for 180 SDR 17.6 pipe at an initial crack tip pressure of 2.05 bar. This pressure profile has been used in uncoupled SwRI's FE code for a crack velocity range of 100 to 175 m/s . The pressure distribution behind the crack tip, given by Fig. 8.3, can be defined using a second order polynomial equation as ($x \approx \text{cm}$ and $p \approx \text{bar}$)

$$p(x) = 1.9507 - 0.0456 x + 0.000275 x^2 \quad (8.9)$$

With these conditions, the axial stress at the pipe (crack tip) is calculated from the above model as 59 MPa. The axial stress was also computed by quasi-static FE analysis under the same loading condition and found as 25 MPa at the outer surface, 80 MPa at the mid surface and 135 MPa at the inner surface. The fully dynamic SwRI-FE code gave 54 MPa at outer surface, 75 MPa at the mid-surface and 97 MPa at the inner surface. These results indicate that the cantilever beam model gives a reasonable value for the axial stress at the crack tip.

8.3. Quasi-static Analysis

Quasi-static analysis of the problem could provide a useful guide to highlight the fundamental aspects governing the event. This analysis was performed using a commercial FE software package, called PAFEC, developed by Pafec Ltd. of Nottingham, UK. The areas studied were the deformed shape of the pipe, and the stress profile in the axial and circumferential directions at the crack tip for a particular pressure and pressure history behind the crack tip. A second focus of the study revolved around the changes in the crack tip stress profile as the crack deviated.

In this quasi-static (QS) computational work, eight noded semi-Loof curved shell (quadrilateral plate) elements were used (Pafec element number is 43210, Pafec Manual 5.1, 1984). For optimum accuracy and maximum reduction in computing time, the mesh was moderately refined along the top of the pipe. The package can describe the general response of the structure as well as indicate the intensity of stress concentration at the crack tip. In this

numerical QS analysis, 180 SDR 17.6 pipe having 1.05 m length was used; the length of the pipe behind the crack tip was 0.6 m. The pressure at and in front of the crack tip was assumed to be 2.05 bar, while the pressure behind the crack tip decreased to 20% of its original value at a distance of about $3D$ behind the crack tip. This pressure profile is the same as that considered in the cantilever beam model as shown in Fig. 8.3. For this loading case the values of stresses and displacements at the axial, circumferential and radial directions were printed out and also displayed graphically.

The mesh and related restraints used in this quasi-static computation are shown in Fig. 8.4. As shown, the pipe was encastred at its end. Since there was no experimental data for the pressure history behind the crack tip, the model proposed by SwRI was used, making it possible to compare the stress results from the SwRI-FE code and those from QS-FE. It is worth pointing out here that the PAFEC package calculated the stresses at each node, but the SwRI-FE code calculated stresses on each element.

8.3.1. Displacement considerations

The deformed shape of the pipe under this loading condition is shown in Fig. 8.5. The displacements at the nodes located on top of the pipe along the crack path behind and in front of the crack tip are shown in Fig. 8.6. A careful analysis of the figure indicates that the displacements in the radial direction and in the circumferential direction are more or less the same behind the crack tip but in front of the crack tip, although the displacements in circumferential direction are zero, there are negative displacements in radial direction, indicating the ovalisation of the pipe. The ovalisation starts at a distance about $0.5D$ behind the crack tip, takes a maximum value at the crack tip of about $0.06D$, and finally reduces asymptotically and the ovalised pipe becomes circular again after a distance of about $1.75D$ in front of the crack tip.

Negative axial displacements observed both behind and in front of the crack tip might be due to the flaring of the flaps behind the crack tip causing an axial stretching. As a result of the pressure behind the crack tip, the pipe wall immediately behind the notch tip undergoes a

large displacement to form two symmetrical 'flaps'. Fig. 8.6 shows that, after a distance of about $3.5D$ distance behind the crack tip, the maximum radial and circumferential displacements are about $0.35D$, thus the total opening at the top of the pipe is about $0.7D$.

8.3.2. Stress Considerations

Since one of the main purposes of this analysis is the examination of the stress variation around the crack tip, the observations are summarised below.

8.3.3. Variation of Axial Stresses Along the Axial Direction

As shown in Fig. 8.7 and Fig. 8.8, the magnitude of the stresses varies not only in the axial and radial directions but also through the thickness. Behind and in front of the crack tip the axial stresses exceed the yield stress of the material and, for this particular loading and pipe dimensions, are around 130 MPa, 80 MPa and 25 MPa at the inner, middle and outer surfaces of the pipe, respectively. If the yield stress of the material is assumed as 25 MPa (under low strain rate) the stresses around the crack tip at the inner surface is about 5.2 times higher than this yield stress, for the middle surface the ratio is about 3.2, but for the outer surface the computed stress which is almost constant in both direction is around the yield stress of the material.

As far as RCP in PE gas pipes is concerned, from Figs. 8.7 and 8.8, one can ask the question: how important are the residual stresses in plastic pipes (which are only about 1-6 MPa) since the stress difference throughout the thickness is about one to two orders of magnitude higher.

8.3.4. Variation of Circumferential Stresses Along the Axial Direction

The quasi-static analysis shows that the circumferential stresses at the top of the pipe shows similar trends in the axial direction as do the axial stresses, but their respective magnitudes at the outer, middle and inner surfaces are much smaller (Fig. 8.8). Although the circumferential stresses are about $2/3$ of the axial stresses, they still exceed the yield stress at low strain rate.

The effects of this stress profile can be seen on the fracture surface in that, (as discussed in Chapter 3) although there is not a unique crack front shape (it varies with the position of the crack tip on the oscillating crack path) the crack tip tends to lead on the inner surface, taking the form plotted in Fig. 3.6.

Both the axial and circumferential average (mid-surface) stress contours around the crack tip are shown in Fig. 8.9 and Fig. 8.10, respectively. These two figures clearly show that, although the circumferential stresses are concentrated around the crack tip and very localised, the axial stresses are distributed more evenly, especially in front of the crack tip, and have higher values.

8.3.5. Principal Stresses

It has long been known that a crack tends to propagate in a direction perpendicular to that of the maximum principal stress. From this point of view, it was thought a plot of the direction of the principal stresses around the crack tip would be informative. This was done for two cases: a straight crack and a deviated crack.

Fig. 8.11 shows the direction of principal stresses for a straight crack indicating that the principal stress direction is generally parallel to the crack direction. From this figure one could predict that under this loading condition the crack will deviate and propagate perpendicular to the maximum principal stress. However, during crack propagation the stress field at the crack tip is continuously modified, and as the crack deviates the maximum principal stress direction in its vicinity will change. The variation of these principal stress directions has been studied, under the same loading conditions, by deviating the crack from its original direction by about 25° and 47° . Fig. 8.11.a,b, and c, show that for curved cracks the principal stress direction becomes almost perpendicular to the crack direction.

8.4. Numerical Dynamic Fracture Models

In general, RCP simulation through numerical methods is not very common, especially for pressure vessels and pipelines where the flow of fluid as a contained medium and crack

driving mechanism complicate the analysis. Another hurdle in the analysis is the modelling of the visco-elastic response of the PE pipe material.

8.4.1. SwRI Program

A computational method has recently been developed and is currently being used by SwRI for the simulation of rapid crack propagation in PE gas pipelines. The program has two different versions: the 'uncoupled' one in which the pressure distribution on the pipe wall is based upon an analysis of the available experimental data, and the 'coupled' one in which all the gas dynamics calculations are carried out simultaneously by a special code. The essential features of the fluid dynamics and the structural dynamics codes in the program are summarized below.

8.4.1.1. Fluid Mechanics Model

In the 'coupled' version of the FE code, the gas dynamics calculations are done by a three-dimensional finite difference code called FLOW3D. The code was modified by SwRI to account for the obstacles that affect the flow (pipe wall) and to allow movement of these obstacles during RCP. Nonetheless, these calculations can only be carried out on a high capacity computer such as a CRAY. To attain the steady-state gas dynamics requires extensive computational time.

In the 'uncoupled' version, these calculations are minimized, making it possible to run the program on a CYBER 960 available at I.C.

8.4.1.2. Structural Dynamics Model

To model the large deformations and rotations of a thin shell, a finite element code, WHAMSE, was selected and modified by SwRI to include crack propagation capabilities. The WHAMSE code has three-dimensional, elastic-plastic and large deformation shell analysis procedures. This nonlinear shell analysis code uses four node quadrilateral elements with a single point integration and hourglass control. A lumped mass matrix is used and crack propagation is simulated using a node release procedure; the stress intensity factor

being determined by calculating the work done by the node release forces. The program was modified at IC to enable the visco-elastic behaviour of PE to be modelled. More details on this code are given in Kanninen *et al.* (1987)

Using this code, SwRI proposed the following relationship to predict the minimum permissible pressure to sustain a propagating crack:

$$K_{max} = 0.725 \frac{p_l}{2} \left(\frac{\pi D}{2} \right)^{1/2} (SDR - 1)^{0.42} \left(\frac{D}{D_n} \right)^{0.67} \left(\frac{E_D}{E_S} \right)^{0.79} \quad (8.10)$$

where, E_S is the material's static elastic modulus (was assumed to be 689.5 MPa), E_D is the dynamic elastic modulus, and D and D_n are the mean diameter and reference distance (taken as 25.4 mm), respectively. p_l is the line pressure (MPa). Since the arrest condition occurs when:

$$K_{max} \leq K_A \quad (8.11)$$

where K_A is a material property whose value was estimated via the Charpy impact tests, using the following relation:

$$K_A = A (E_S C_V) \quad (8.12)$$

where A is a constant determined from experimental results and C_V is the Charpy impact energy density. The energy release rate can be calculated from the stress intensity factor using the well-known relation:

$$G = \frac{K^2}{E_D} \quad (8.13)$$

From Eqs. 8.10, 8.11, 8.12 and 8.13, the minimum Charpy impact energy for the PE material at to arrest a propagating pressure could be calculated through the relation:

$$(C_V)_{min} = \frac{K_{max}^2}{A^2 E_S} \quad (8.14)$$

in which the value of $A=8.3$ (dimensionless) was suggested.

8.4.2. Computational Results from the SwRI-FE Program

For the present research program, the computer simulation of RCP in PE pipe developed by SwRI has been used in two ways. The first consisted of test runs in which the pressure behind the crack tip was calculated by the uncoupled version for the velocity range that was considered. Secondly, the program was modified to simulate the present test technique in order to interpret the effect of different parameters influencing RCP in the PE pipe.

8.4.2.1. Uncoupled Calculations

The program was originally run on a microVAX computer at SwRI. After some modification it was successfully run on the CYBER 960 main-frame at IC. The uncoupled version of the FE code was used for two different pressure models giving the pressure history behind the crack tip. They are SwRI's, and Alder's pressure models (discussed in Chapter 2).

a) Results from SwRI's pressure model: In this analysis the dynamic elastic modulus and Poisson's ratio are taken as $2.65 \times 10^9 \text{ Nm}^{-2}$ and 0.42, respectively. For this test-run, the crack tip pressure and the crack velocity were taken as 2.05 bar and 145 ms^{-1} , respectively, and the pressure variation behind the crack tip was calculated. In Fig. 8.12, the dynamic crack driving force accompanying RCP in 180 mm SDR 17.6 PE pipe pressurised by air is shown as a function of the crack propagation length. The figure shows that after a certain crack propagation length from initiation, a steady-state condition is reached; the value of K (or G) at this 'plateau' is the key result.

Although there is a small increase in G as the crack approaches the pipe end, this could be neglected. The figure also shows that to attain a steady state value, the crack has to propagate about $7.5D$. After this distance the calculated G value under the selected test condition is about 24 kJm^{-2} . Using the test parameters, the G value from the Irwin-Corten equation is 0.347 kJm^{-2} , which is only about 1.45% of the computed value. When the line pressure calculated from Eq. 2.6 is put into the strain energy equation, the corresponding G value is found as 1.391 kJm^{-2} which is only about 5.8% of the computed value.

Further computational results have shown that the crack propagation distance required to attain the plateau value varies with crack velocity, pipe diameter and the crack tip pressure. One of the most significant results of this model is the existence of a maximum crack driving force for any given set of operating conditions and pipe geometry. As shown schematically in Fig. 8.13, the crack driving force increases with the crack velocity but after a maximum it starts to decrease again. The initial increase in crack driving force is, presumably, due to increasing pressure at the crack tip. The decrease after the peak is apparently due to the increasing inertial effect of the pipe wall behind the crack tip.

Using the same parameters used in 180 SDR 17.6 pipe, the variation of crack driving force was also calculated for 180 SDR 11 pipe and the result is shown in Fig. 8.14. There are two important points worth mentioning about the figure. Firstly, after attaining a maximum value of about 5.5 kJm^{-2} the calculated crack driving force (G) decreases to as low a value as 2.8 kJm^{-2} . Although the reason(s) for this declining crack driving force is (are) not well understood, it is thought that the closure of flaps well behind the crack tip may cause this. The second point about the figure is that the mean value of G is about 4 kJm^{-2} , which is only 16% of G for 180 SDR 17.6 pipe. From both FS and SS test results, it can be understood that as the thickness goes up there is only a small increase in critical pressure. Thus, using the SwRI's pressure model, the numerical and experimental results disagree with each other. From this observation it could be pointed out that the pressure history behind the crack tip cannot be expressed only as a function of crack velocity, but also as a function of the pipe wall thickness. As mentioned in the previous Chapter, the thicker the pipe, less the flaps open (due to increased stiffness). The pressure lost behind crack tip, therefore, becomes smaller, consequently the steady-state G value becomes higher.

b) Alder's pressure model: In order to calculate the crack driving force during RCP precisely, the pressure history behind the crack tip has to be defined with respect not only to the crack velocity but also the diameter and thickness (as well as stiffness and backfill). However, the lack of such a complete model taking into account all the above parameters is one of the major

obstacle for numerical modelling. Since Alder's model defines the pressure history with respect to the flap opening angle, the SwRI's FE program was modified to include this pressure model as an option to be run when it is required.

The calculated crack driving force from Alder's model and SwRI's model for 180 SDR 11 and 17.6 is given in Figs. 8.15 and 8.16, respectively. From these figures it is seen that the driving force from Alder's model for 180 SDR 11 is about double that from SwRI's model. On the contrary, for 180 SDR 17.6 both models give more or less the similar crack driving force values. In the run using Alder's model, the crack driving force attains the steady-state slightly earlier. Nevertheless, from the present S4 test results and the FS test results it is known that decreasing SDR (*i.e.* increasing the thickness) does not have a strong effect on the critical pressure. These results highlight the important fact that there is not a unique pressure profile behind the crack tip but it varies with the thickness of the pipe. Thus, for thicker pipes the rate of pressure decay is smaller compared to that for thinner pipe. From this point of view, the Alder's pressure model could simulate the pressure profile more accurately.

8.4.2.2. Stress State Around a Propagating Crack

The stress state around a propagating crack tip was calculated with SwRI's uncoupled FE code. For these calculations the pressure given in Fig. 8.3 was used. It has to be pointed out that the program calculates not the stresses at each nodal point but at the central point of each element. However, the program does calculate the stresses at the inner, middle and outer surface.

The calculated axial stress state along the axial direction is plotted in Fig. 8.17 showing a high stress gradient through the thickness around the crack tip. The inner stresses around the tip of the crack are about 1.8 times higher than at the outer surface. The variation of circumferential stresses at the crack tip plane around the circumference is given in Fig. 8.18. As can be seen, the axial stresses become zero after about 45° from the crack tip, and afterwards attain negative values, Fig. 8.19.

In Fig. 8.19, the circumferential stress variation in the axial direction along the top of the pipe is given, showing the stress gradient through the thickness. The comparison of the figure with Fig. 8.17 shows two important points. Firstly, the circumferential stress around the crack tip is only about $1/3$ of the axial stress. Secondly, although the axial stresses are significant to within $1D$ either side of the crack tip, the circumferential stress is localised to within $0.2D$ either side of the crack tip. The variation of circumferential stresses around the circumferential direction is given in Fig. 8.20.

8.4.2.3. Modelling of S4 Pipe Test Technique

The SwRI-FE code was modified to incorporate ring constraint to simulate the present test technique. For these runs the pressure in front of, and behind, the crack tip was assumed to be constant and the flaring of flaps was restricted by the presence of the ring.

For 2.05 bar pressure and 3.81 mm clearance throughout the pipe, the variation of G along the crack propagation distance is plotted in Fig. 8.21. Of special importance is the observation that G attains a plateau in a relatively short time, typically corresponding to about $2.5D$ after initiation, showing the contribution of the containment cage used in the experiment towards the attainment of conditions necessary for steady-state crack propagation. Another important feature of the figure is the oscillating crack driving force during crack propagation.

By varying the gap between the pipe outside diameter and the cage inside diameter from 1.27 up to 6.35 mm, the significance of clearance in relation to the steady state G value was studied. The results of these analyses are shown in Fig. 8.22, illustrating three significant trends. The first one is that as the gap increases the value of crack driving force increases. In contrast to these numerical results, as explained in the previous Chapter, increasing the gap increases the critical pressure, or, to put it another way, higher clearances tend to decrease the critical energy release rate at fixed pressure. The main explanation of these somewhat contradictory results could be due to two facts:

(i) In the numerical analysis the pressure behind the crack tip is kept constant, but in experiments as the gap is increased there remains more space for the flaps to flare radially as well as circumferentially behind the crack tip which reduces the pressure behind the crack tip and, consequently, reduces crack driving force.

(ii) In the experiments, as the pipe flaps behind the crack tip are allowed to flare out more, axial and circumferential stresses at the crack tip zone increase. For ductile materials (such as PE), having higher axial stresses around the crack tip could increase the plastic zone at the crack tip and accordingly, increase the resistance of the material to crack propagation. If that is the case, for a particular crack tip pressure, a larger gap has the effect of increasing the crack driving force but, on the other hand, could increase the material's crack resistance.

It is also worth pointing out that, the magnitude of oscillation in these crack driving force result increases as the clearance is increased. For the time being, the reason for this oscillation is not as yet well understood, but it could be that, as the clearance is increased the flaps hit the containment cage and start bouncing, resulting in an elastic wave propagating through the pipe and varying the value of the crack driving force.

8.5. Discussion on the Numerical and Analytical Results

Both the analytical and numerical studies presented above have improved the understanding of stress state and pipe deformation of RCP in PE gas filled pipe. The analytical cantilever beam model to evaluate the axial stress at the crack tip region seems to be very encouraging. The same model could also be improved to take into account the inertial effects and, may be, to determine the circumferential stress at the crack tip region. It is now obvious that the axial stretching creates very high axial stresses which could be one of the main sources of the pipe wall deformation around the crack tip (ovalisation) and/or crack path instability.

Both the dynamic and quasi-static numerical analyses have shown that both axial and circumferential stresses at the crack tip domain are not uniform through the thickness but attain a maximum value at the inner surface. The effects could be seen on the fracture surfaces and crack front shapes.

The computational results from SwRI's modified dynamic numerical FE code to simulate the S4 pipe technique have shown the importance of the containment cage in obtaining steady-state RCP very soon after initiation. In addition to this, although the calculated crack driving force varied with the gap between the pipe and the cage, the calculated driving force with the cage is more or less the same as that calculated for an unrestrained pipe with decompression, Figs. 8.12 and 8.21. This result seems to be encouraging for correlating the SS test results with those of FS tests by assuming that crack propagation behaviour in the present technique is the same as that in a FS test if the pressure at the crack tip in both tests is the same.

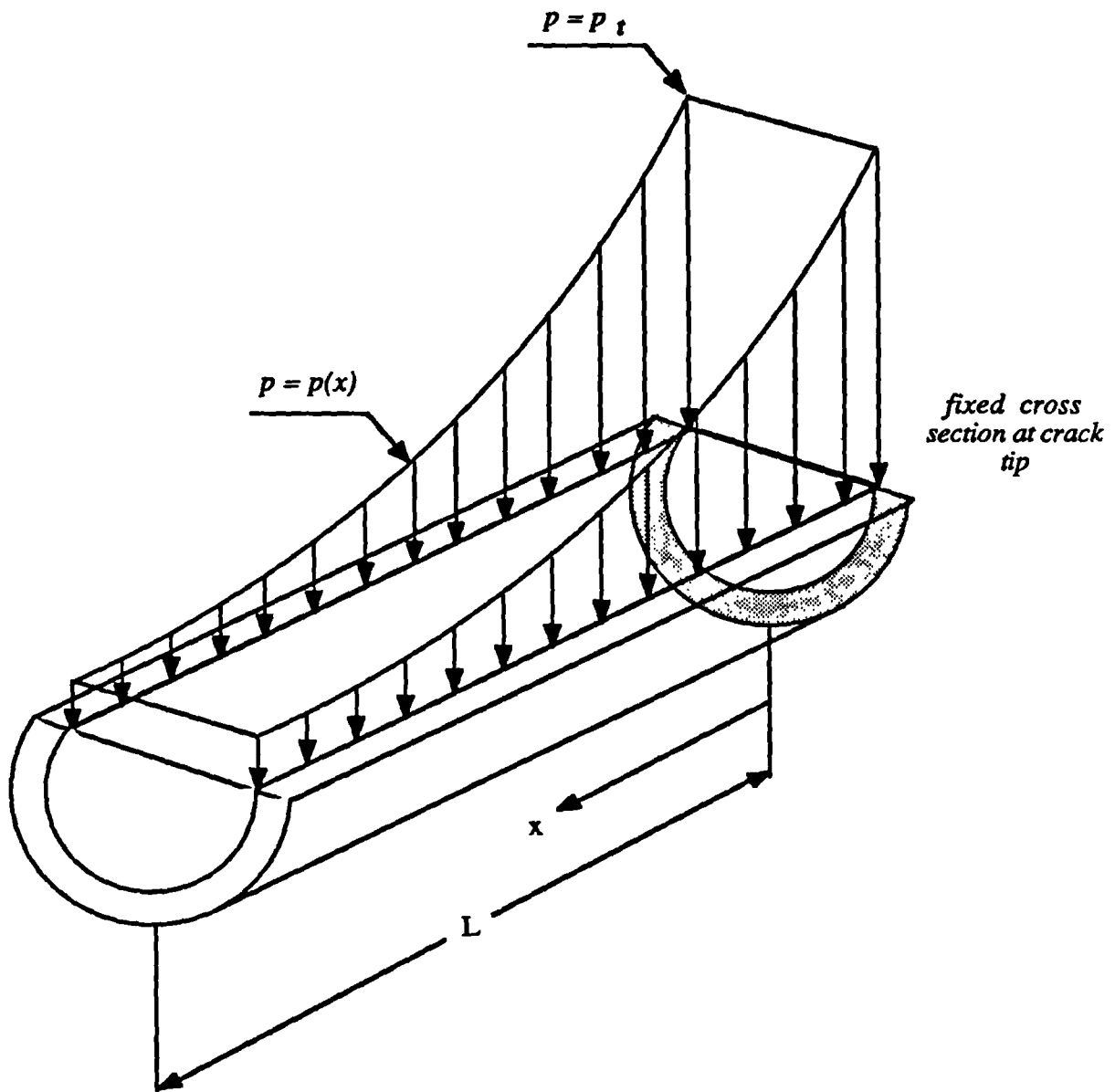


Figure 8.1 Schematic representation of the cantilever beam model.

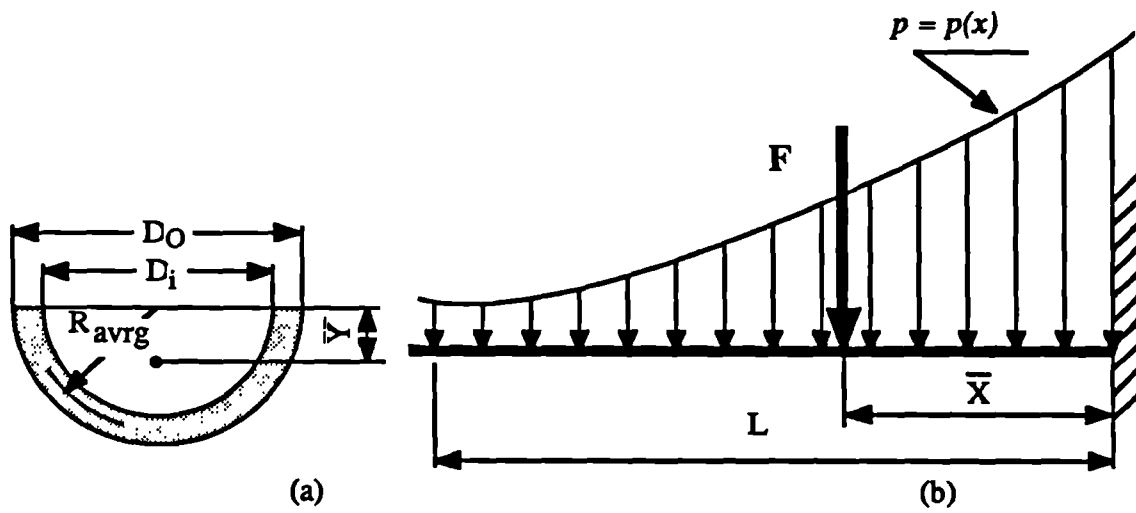


Figure 8.2 Dimensions used in the cantilever beam model.

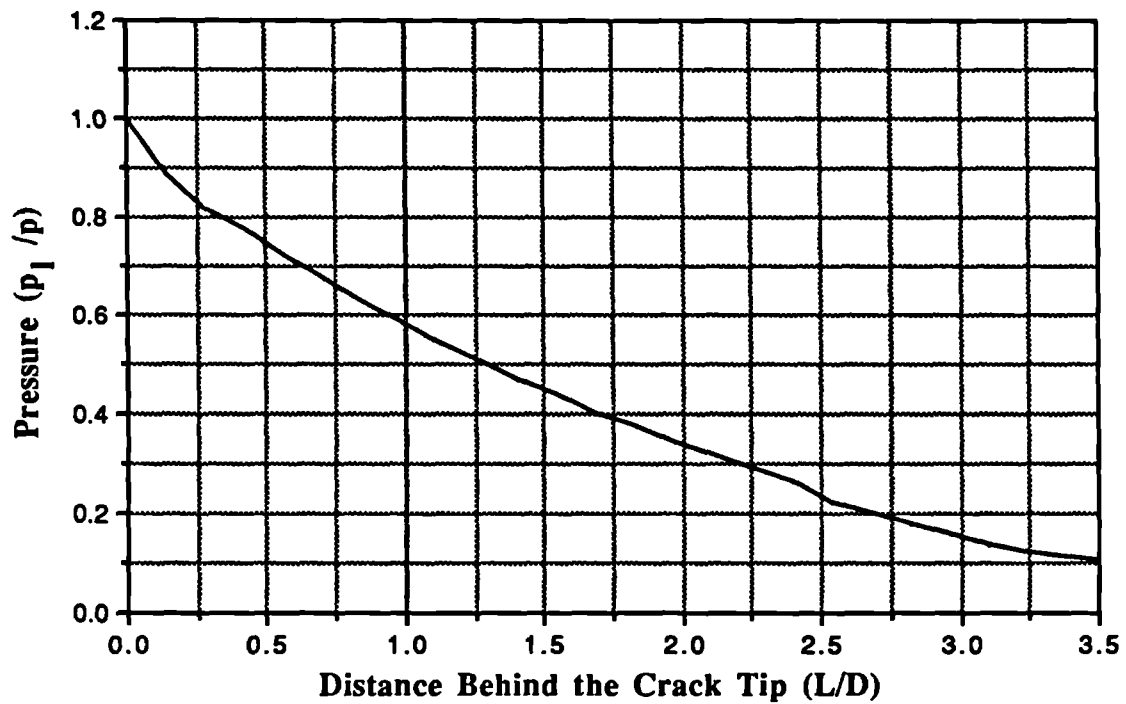


Figure 8.3 Pressure history behind the crack tip taken into account in the theoretical and numerical analyses (which has also been used in the uncoupled version of SwRI-FE code).

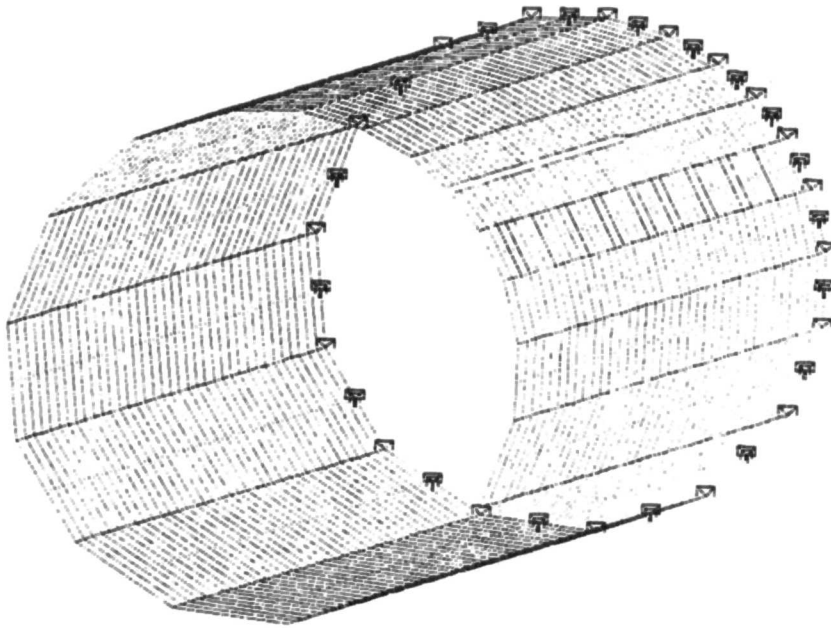


Figure 8.4 Mesh and related restraints used in the quasi-static analysis.

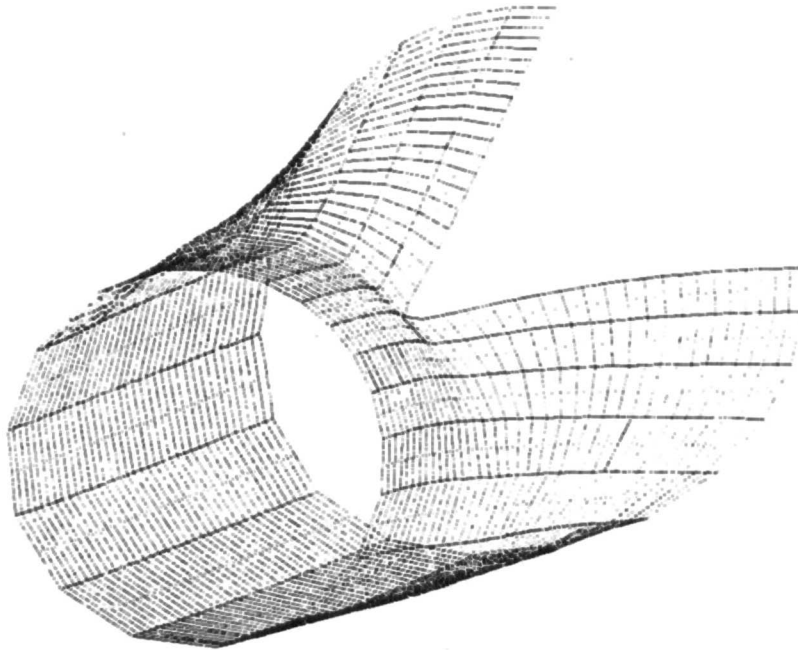


Figure 8.5 The deformed shape of 180 SDR 17.6 pipe for 2.05 bar pressure in front of the crack tip and decaying pressure behind the crack tip (quasi-static FE analysis).

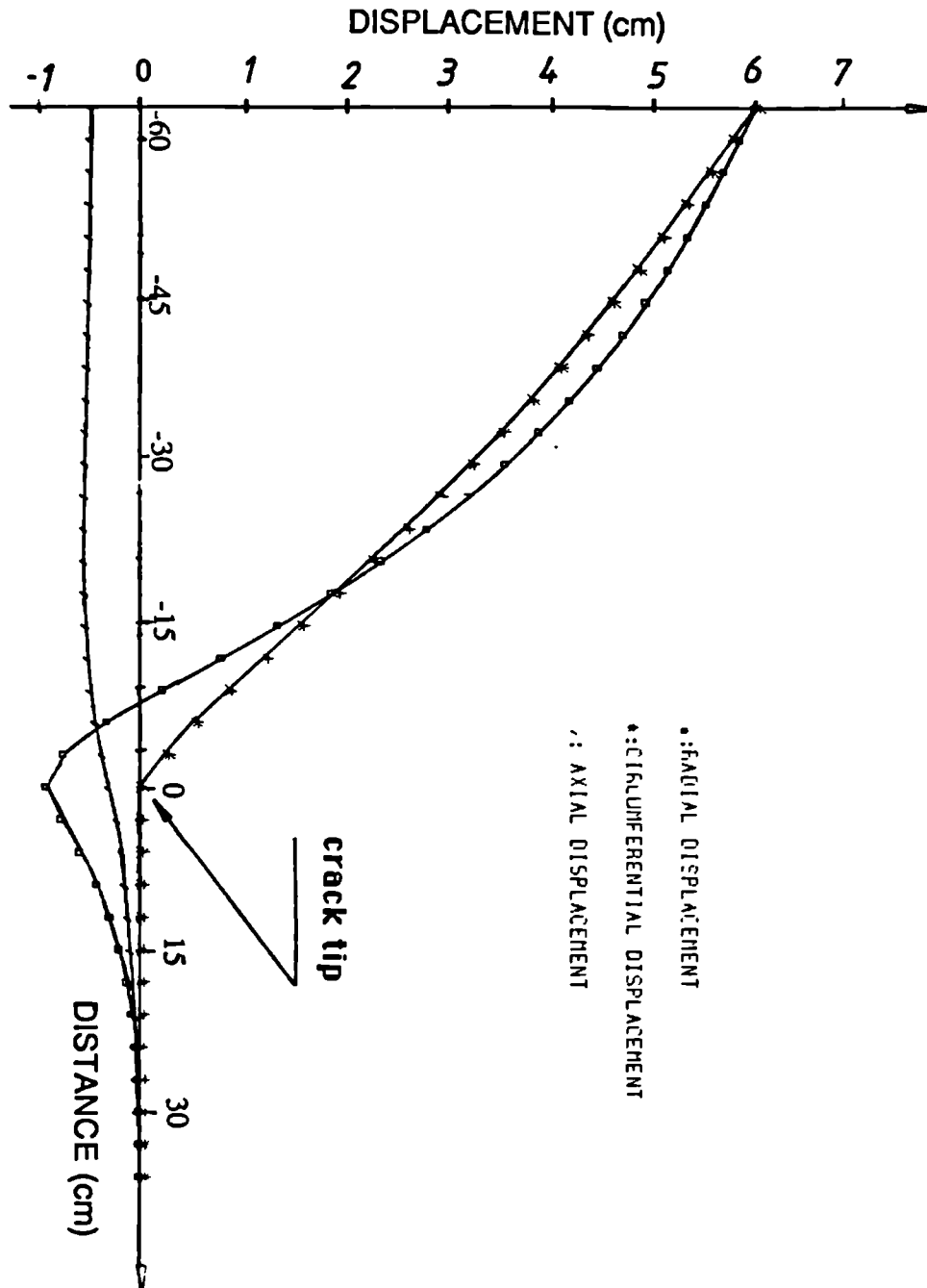


Figure 8.6 Computed displacements for quasistatically loaded 180 SDR 17.6 pipe for 2.05 bar initial crack tip pressure

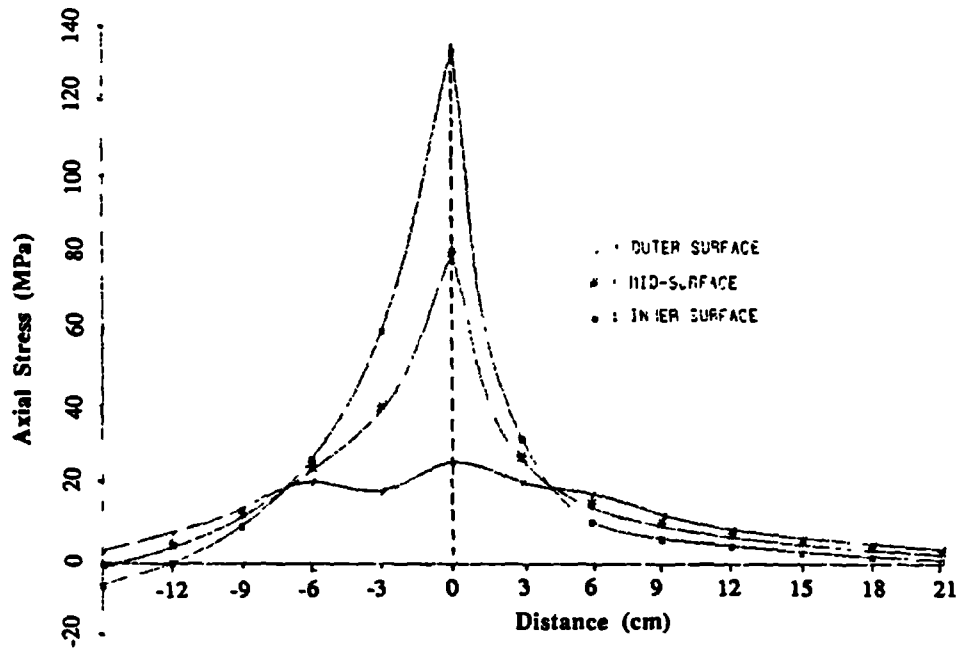


Figure 8.7 Variation of quasi-statically calculated the axial stresses in the axial direction around the crack tip.

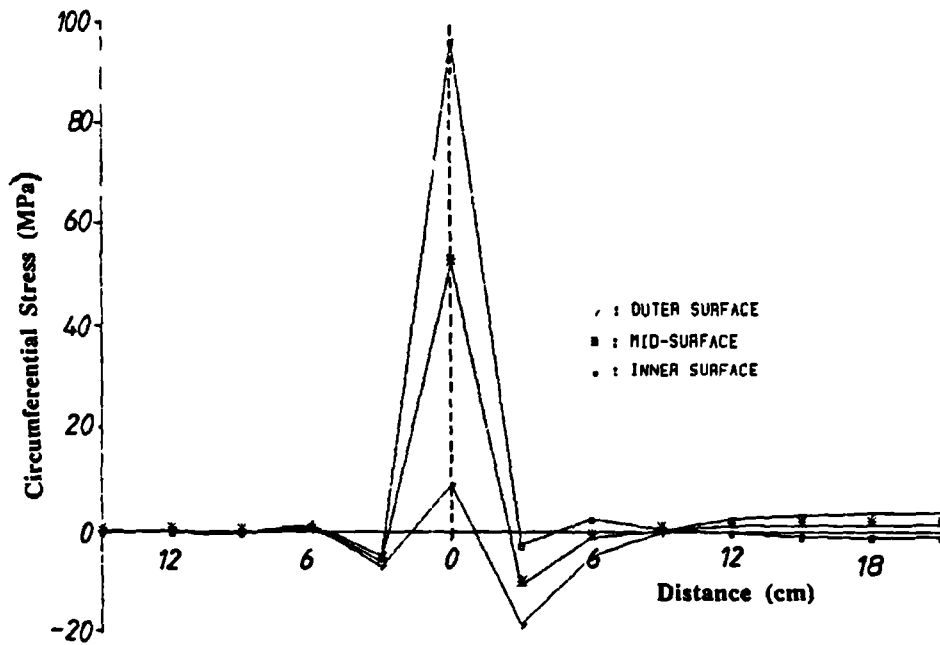


Figure 8.8 Variation of quasi-statically calculated circumferential stresses in the axial direction around the crack tip.

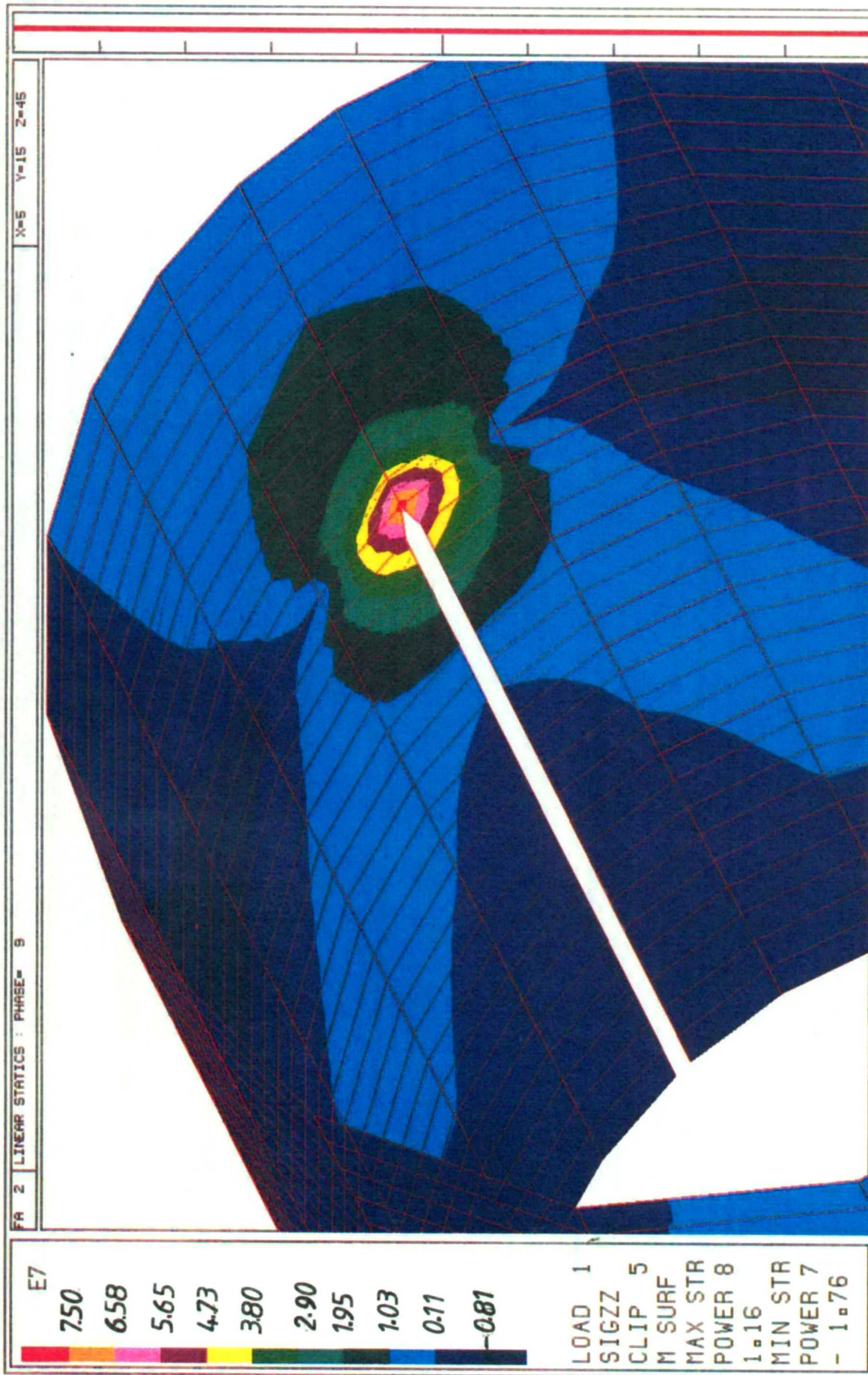


Figure 8.9 Quasi-static FE analysis results showing the axial stress contours around the crack tip (stresses: Nm^{-2})

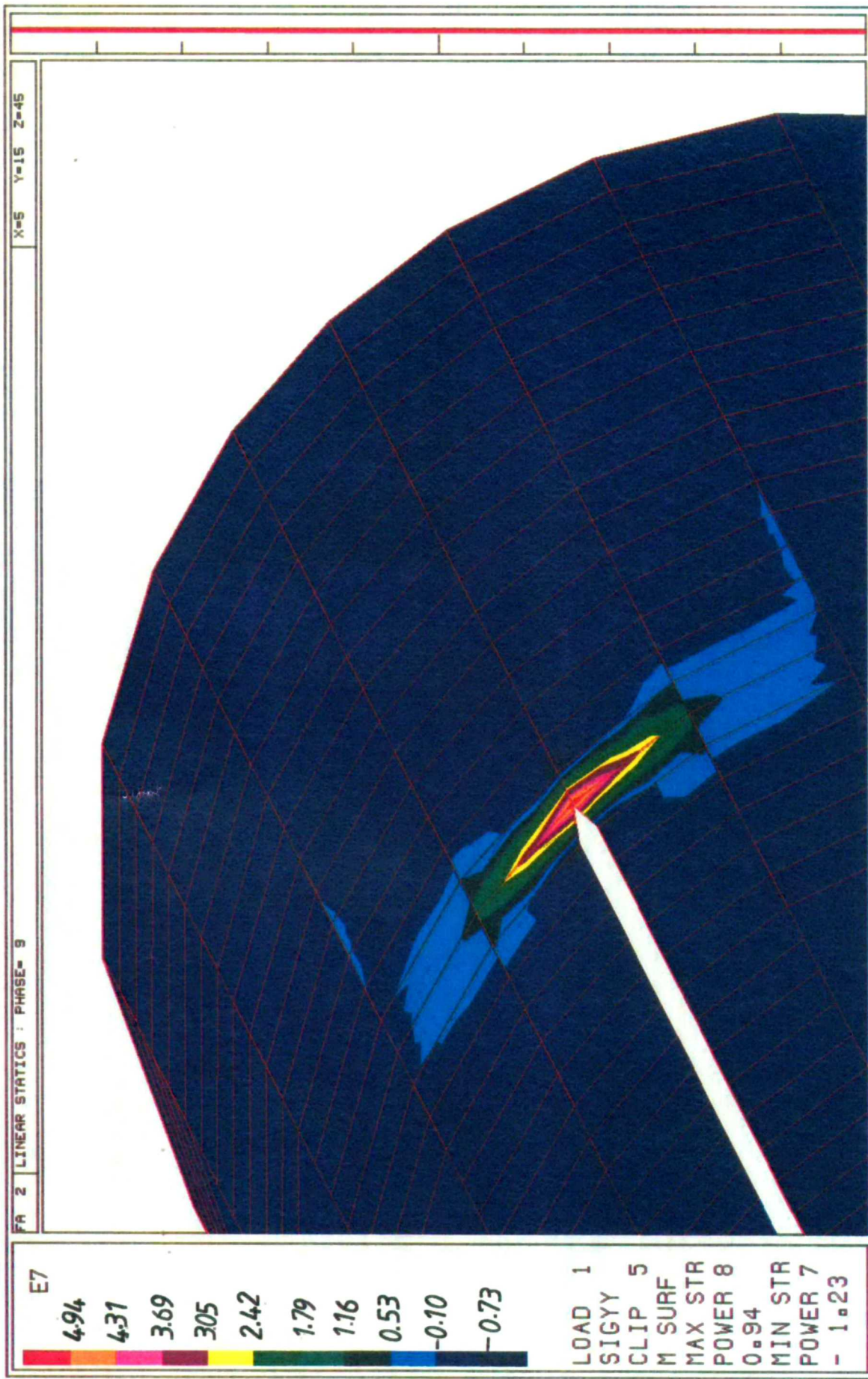
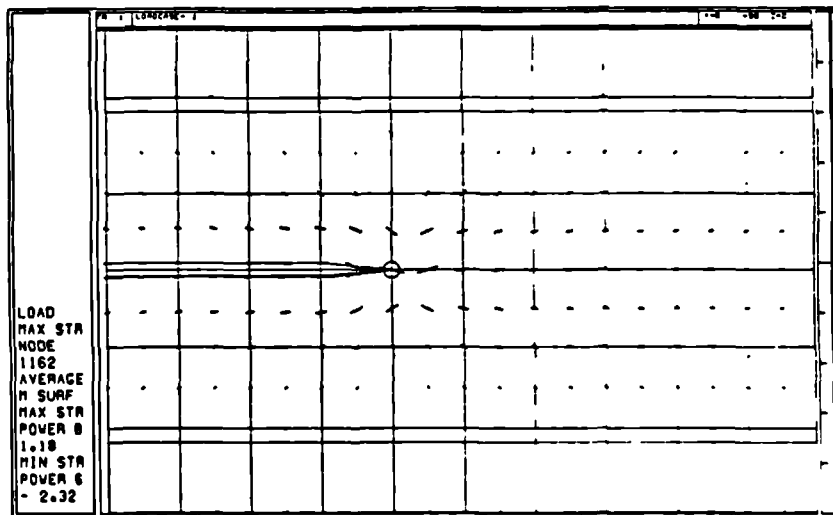
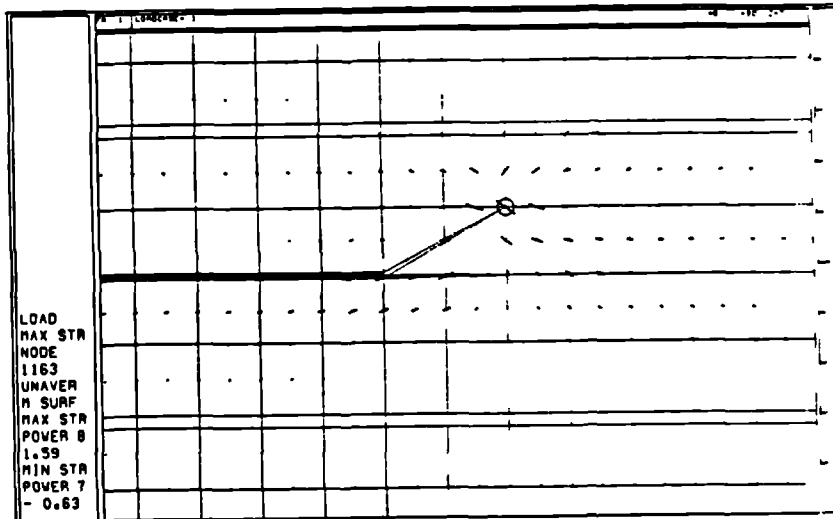


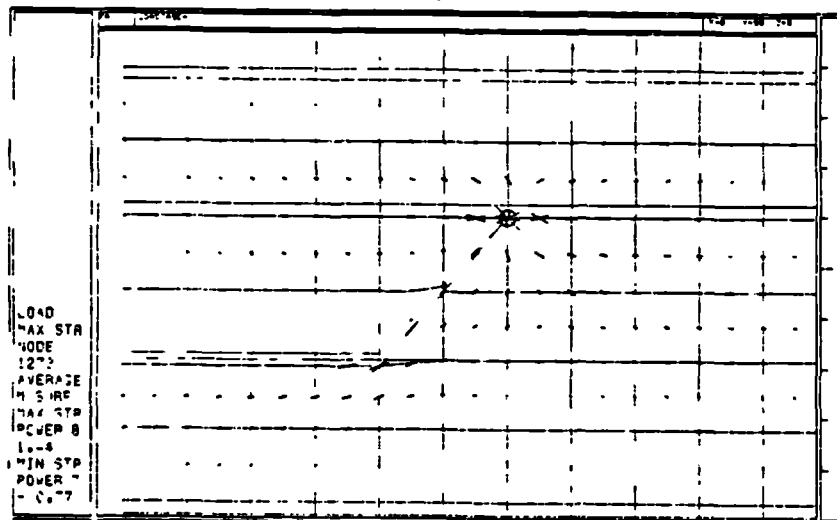
Figure 8.10 Quasi-static FE analysis results showing the circumferential stress contours around the crack tip (stresses: Nm^{-2})



(a)



(b)



(c)

Figure 8.11 Direction of quasi-statically evaluated principal stresses around the crack tip for
 a) Straight crack b) 25° deviated and c) 47° deviated crack from its original direction.

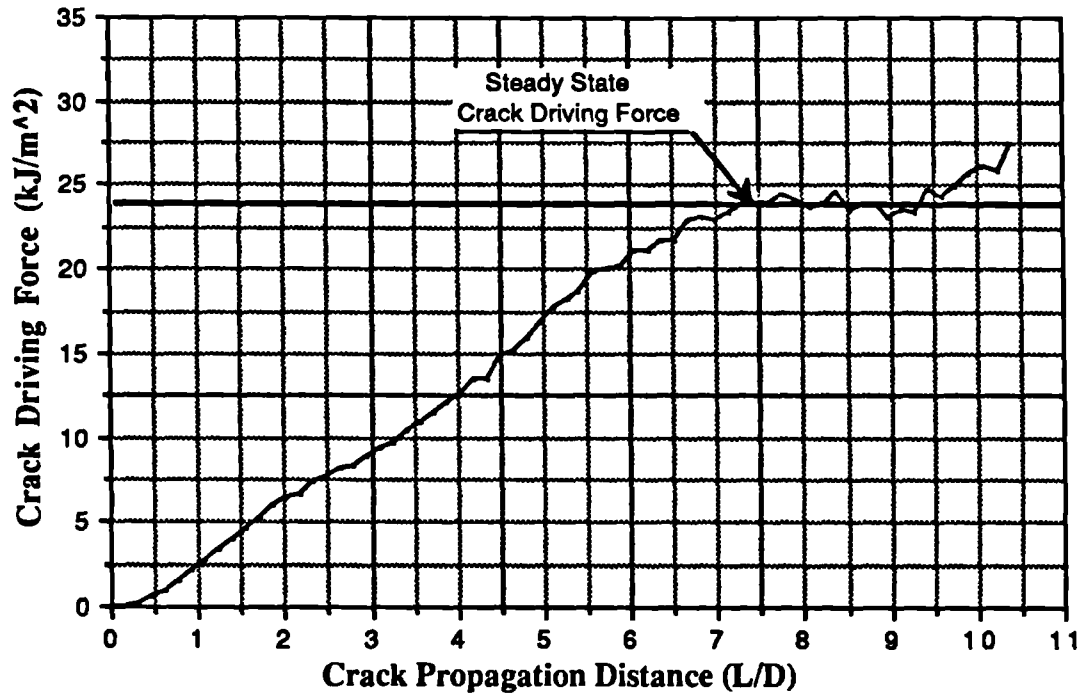


Figure 8.12 Variation of computed crack driving force along the crack propagation distance for 180 SDR 17.6 pipe, showing a steady state crack driving force after about 7D from the initiation. The pressure is calculated by the uncoupled version of the SwRI-FE code for the crack velocity of 145 ms^{-1} and pressure of 2.05 bar.

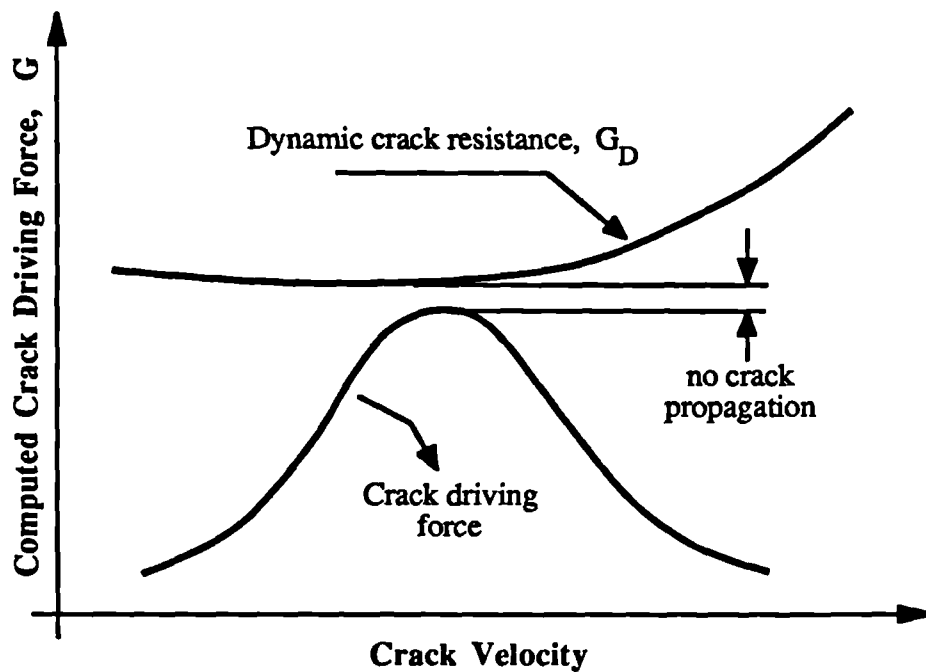


Figure 8.13 Schematic representation of effect of crack propagation velocity upon steady state crack driving force.

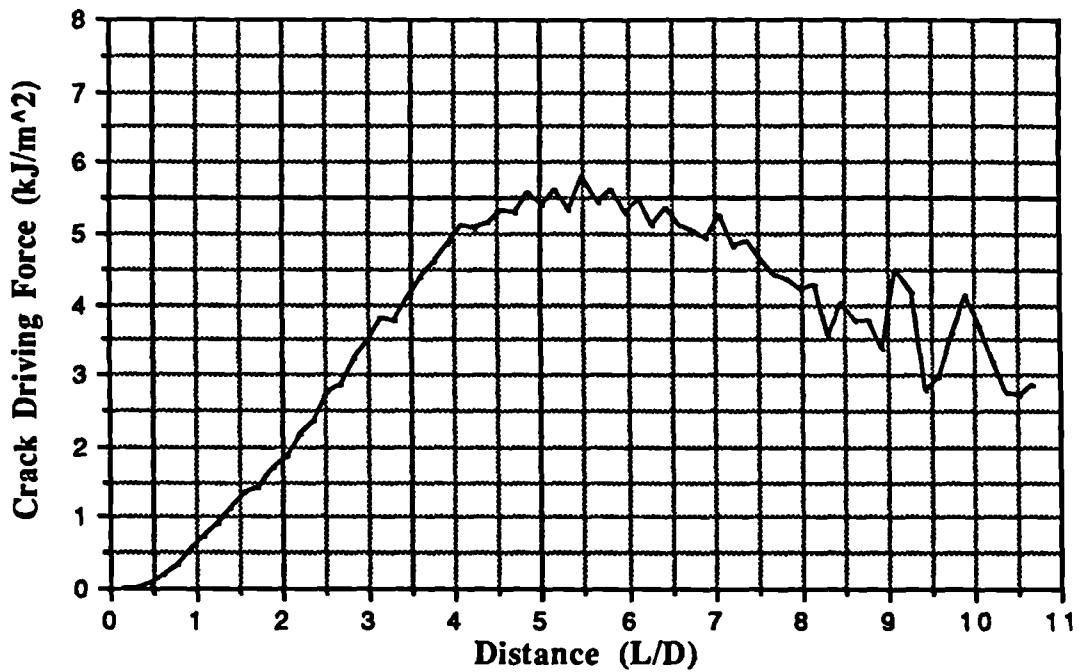


Figure 8.14 Variation of computed crack driving force along the crack propagation distance for 180 SDR 11 pipe, showing a maximum crack driving force of 5.5 kJm⁻² after about 5D from the initiation. The pressure history behind the crack tip is calculated by the uncoupled version of the SwRI-FE code for the crack velocity of 145 ms⁻¹ and pressure of 2.05 bar.

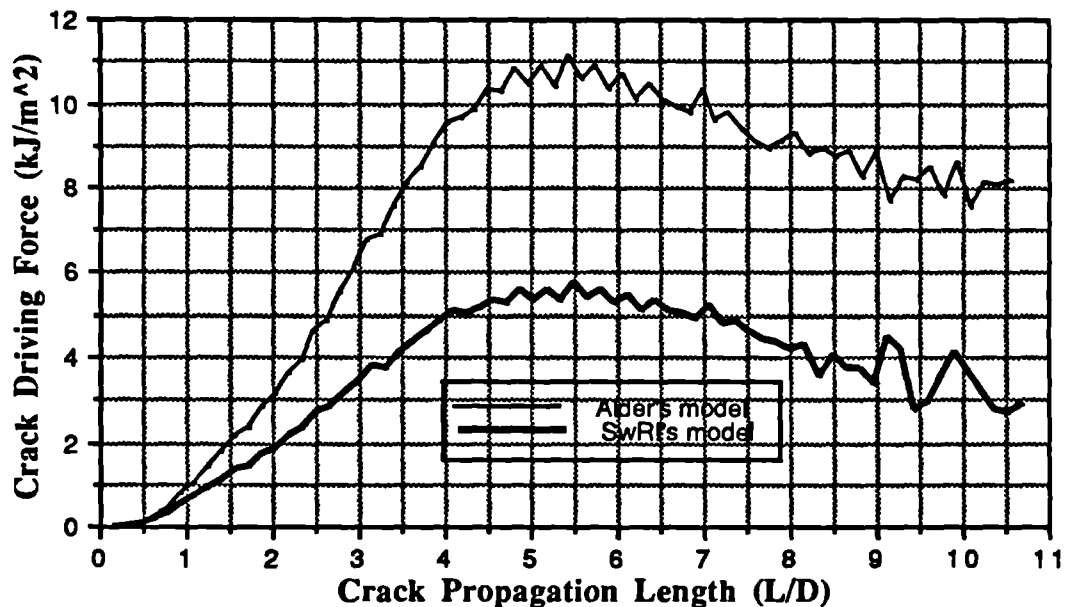


Figure 8.15 The comparison of the variation of computed crack driving force along the crack propagation distance for 180 SDR 11 pipe. The pressure history behind the crack tip is calculated by both the uncoupled version of the SwRI-FE code and Alder's models for the crack velocity of 145 ms⁻¹ and pressure of 2.05 bar.

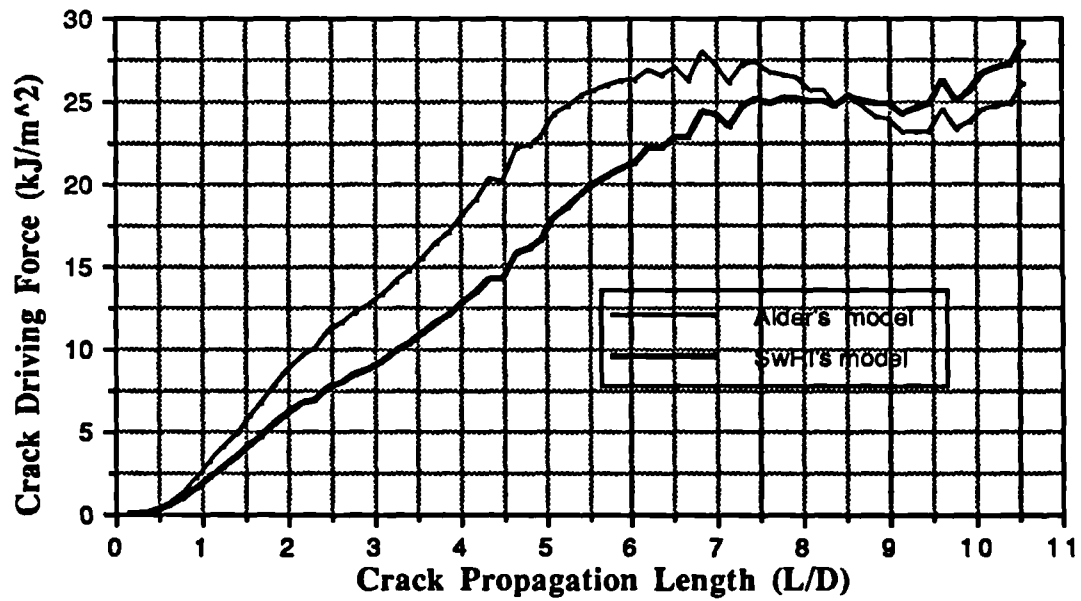


Figure 8.16 The comparison of the variation of computed crack driving force along the crack propagation distance for 180 SDR 17.6 pipe. The pressure history behind the crack tip is calculated by both the uncoupled version of the SwRI-FE code and Alder's models for the crack velocity of 145 ms⁻¹ and pressure of 2.05 bar.

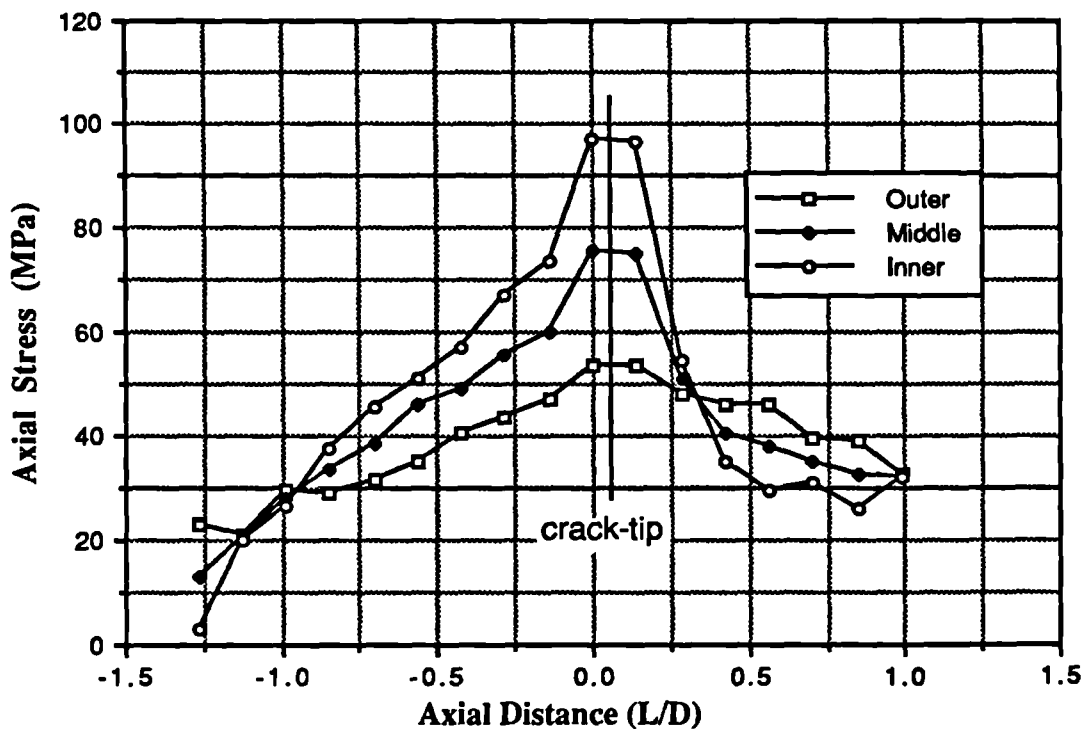


Figure 8.17 Variation of axial stresses at the top of cracking pipe along the axial direction ($p = 2.05$ bar, $v = 145$ ms⁻¹, 180 SDR 17.6).

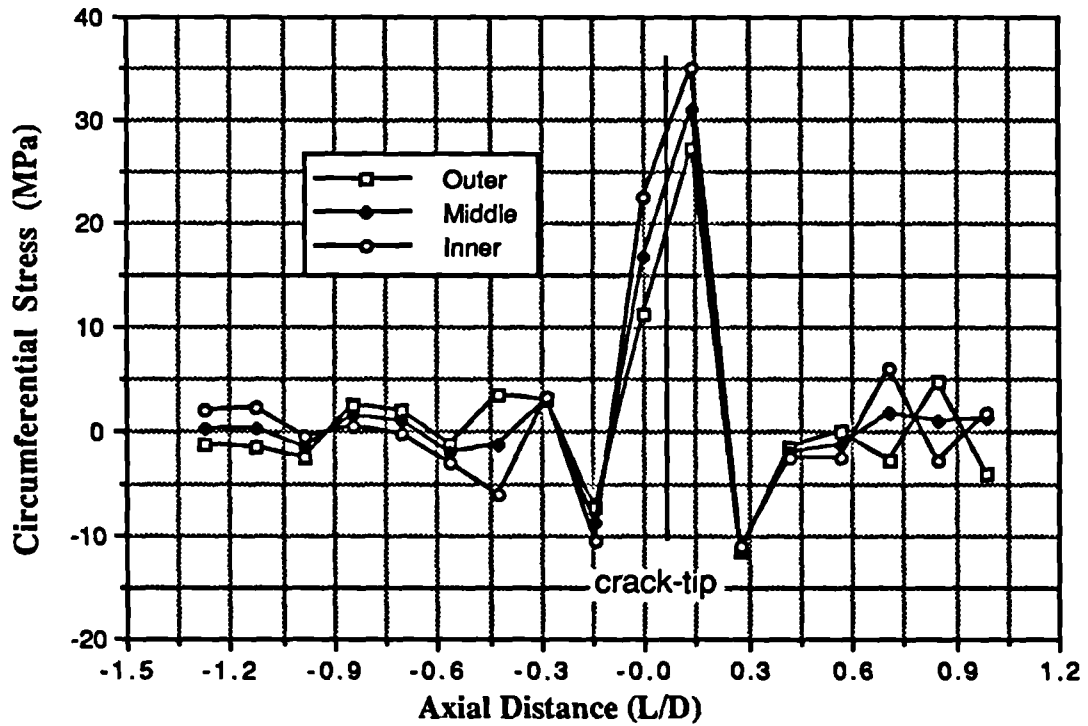


Figure 8.18 Variation of circumferential stresses at the top of cracking pipe along the axial direction ($p = 2.05$ bar, $v = 145 \text{ ms}^{-1}$, 180 SDR 17.6).

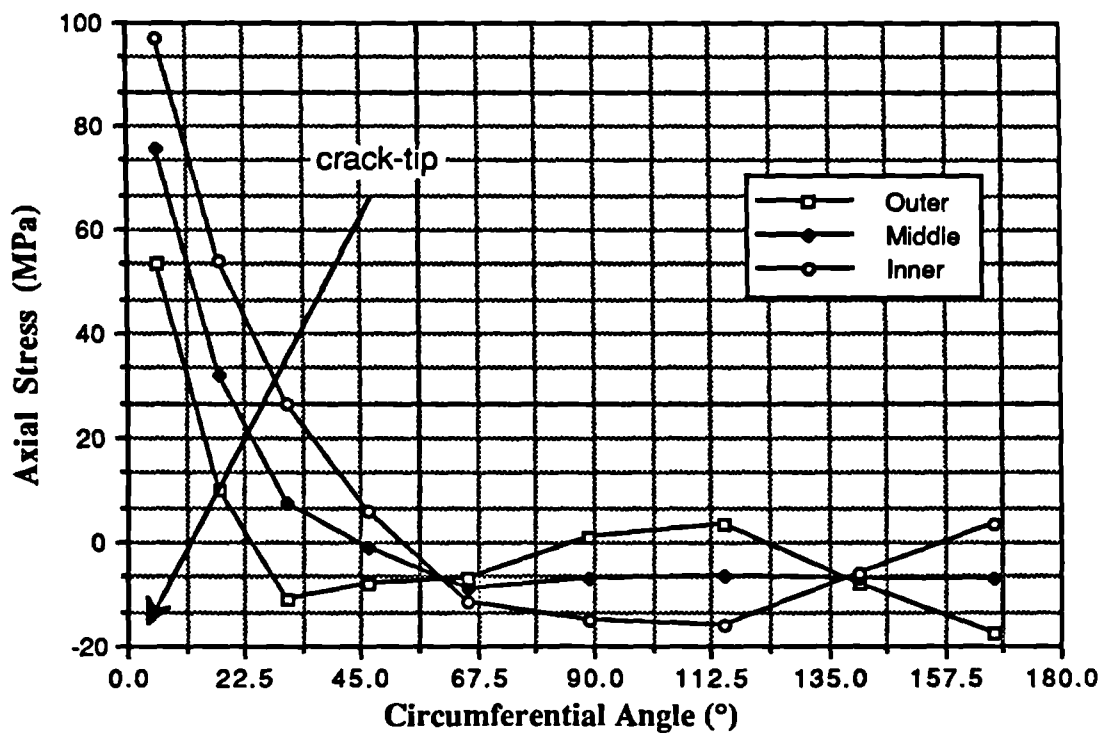


Figure 8.19 Variation of the axial stresses of cracking pipe along the circumferential direction ($p = 2.05$ bar, $v = 145 \text{ ms}^{-1}$, 180 SDR 17.6).

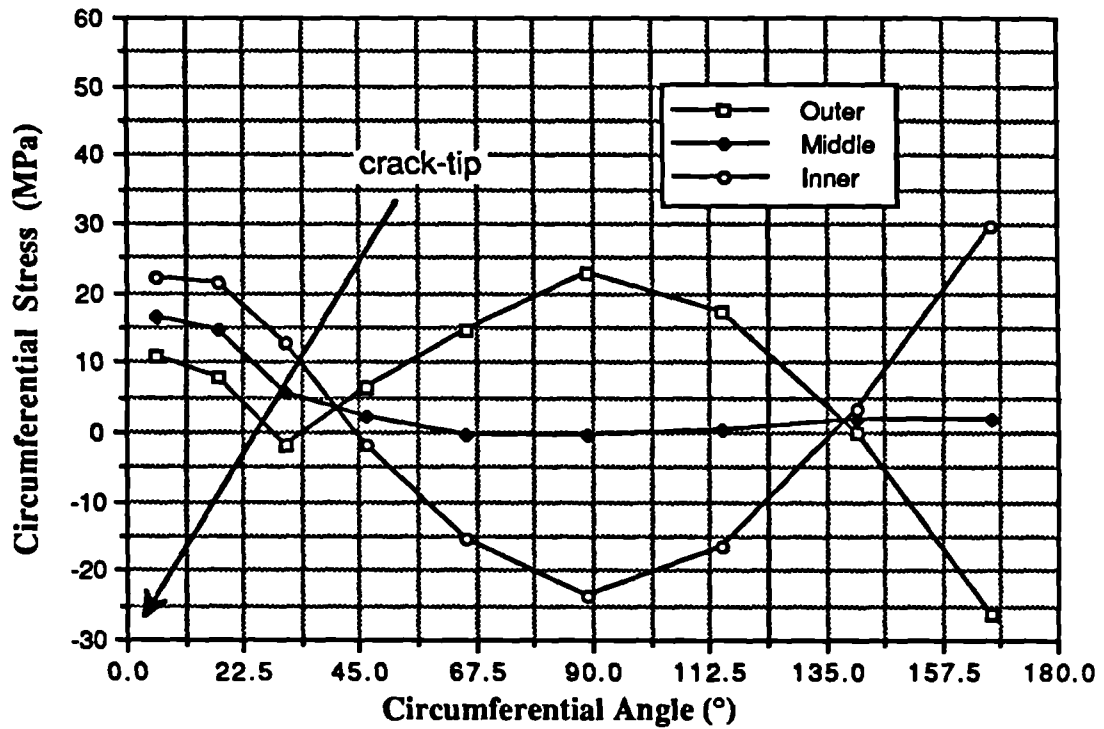


Figure 8.20 Variation of circumferential stresses of cracking pipe along the circumferential direction ($p = 2.05$ bar, $v = 145 \text{ ms}^{-1}$, 180 SDR 17.6).

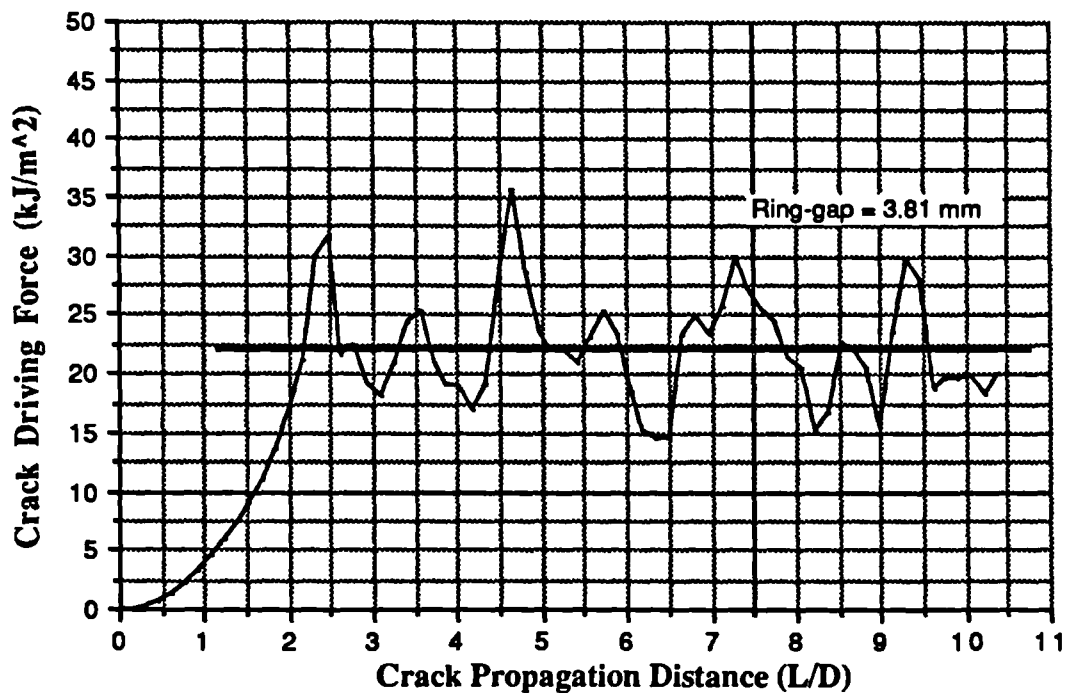


Figure 8.21 Variation of crack driving force along the crack propagation distance for 180 SDR 17.6 pipe for a restrained pipe with 3.81 mm clearance and a constant pressure of 2.05 bar at and behind the crack tip for a crack propagating at a velocity of 145 ms^{-1} .

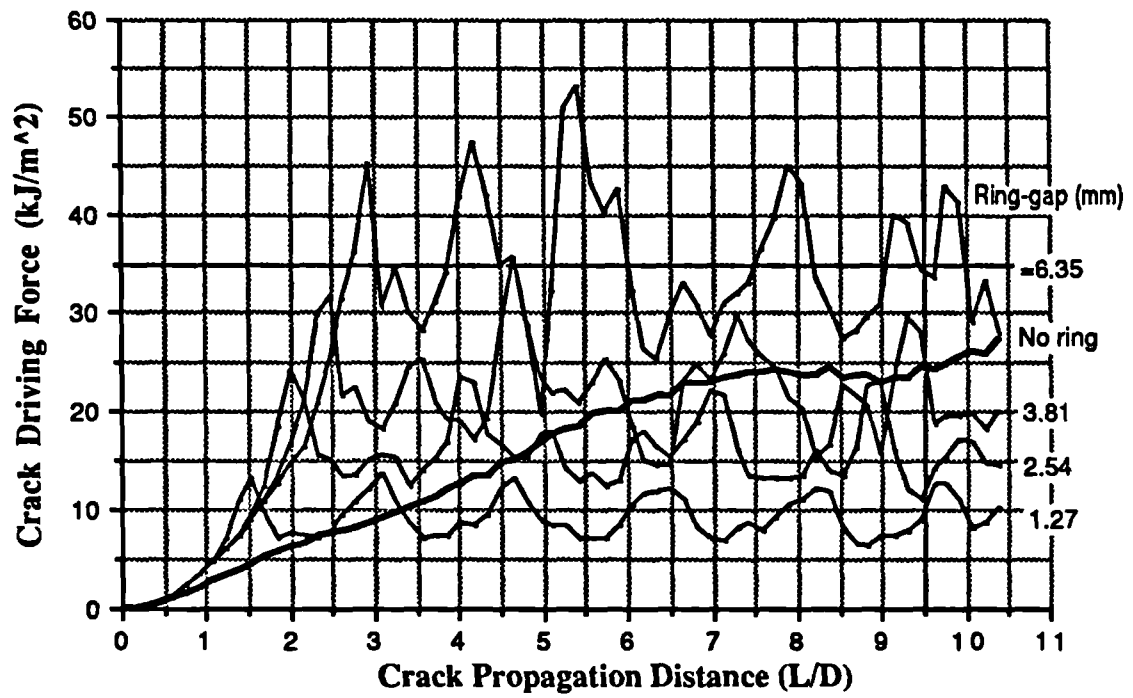


Figure 8.22 Effect of the gap between the pipe and cage upon the steady state crack driving force for 180 SDR 17.6 pipe for a constant pressure and crack velocity of 2.05 bar and 145 ms⁻¹.

CHAPTER 9

CORRELATION OF RESULTS

9.1. Introduction

In Chapter 6, we studied the effects of the parameters that are believed to control RCP in PE pipes. These parameters seem to be temperature, pipe wall thickness, and pipe diameter. Temperature was shown to be the most influential test parameter. The material properties affected by temperature are known to be fracture resistance, elastic modulus and yield stress. The variation of these parameters with temperature is an important, but not easy subject, since different test techniques used to characterise the material could give a wide range of values for each parameter. Secondly, the mechanical response of PEs is known to be thickness as well as strain rate dependent. As a result, it is questionable to generalise a set of data obtained from a given thickness and strain rate to a wide range of thicknesses and strain rates.

In the present chapter, the S4 test results obtained for a wide range of temperatures, thicknesses, and diameters will be used to establish a correlation between the results of

- Full-scale tests,
- Charpy impact tests, and
- High Speed Double Torsion tests.

9.2. Relationship Between S4 and Full-scale Test Results: Decompression Effects

If the technique which we have described here is to be a serious contender as a quality control test, it must show a similar discrimination to pipe stock as is evidenced by the full-scale test. The critical pressure result of S4 tests on 250 SDR 17.6 pipe at 0 °C for MDPE (Finathene), 1.70 bar, is considerably lower than the result of full-scale tests on the same size (6.1 bar for

BP 002-40). The most obvious explanation for this considerably lower result from S4 tests is the process of decompression which accompanies the fracture event in a FS test (see Chapter 2).

Although no pressure variation measurements were taken in S4 tests, we can assume, because of the presence of the baffles and containment cage, that the pressure in front of the propagating crack remains constant during the fracture. Similarly, for the pressure history behind the crack tip, the restraining effect of the containment cage restricts radial flaring of the pipe flaps, which suppresses radial decompression. Additionally, the baffles prevent the axial decompression of the gas behind the crack tip. Thus, it could be assumed that in the S4 test any pressure decay in front of or behind the crack tip is negligible compared to the relatively high drop in FS tests.

If the crack front in S4 test behaves at its initial pressure in the same way as the crack front in a full scale test at its reduced pressure, there would be a 'scaling factor' between them. However, the reduced crack tip pressure in FS tests is a function of the crack velocity and could vary between the original line pressure and 28% of its initial value (see Eq. 2.6). At 70 ms⁻¹, the minimum crack velocity measured in our 0 °C tests on MDPE, this crack velocity dependent crack tip pressure model predicts that the near tip pressure in FS (unbaffled) pipe will be approximately 35% of the initial line pressure. One could assume that the pressure acting on the flaps immediately behind the crack tip has the same contribution in S4 and FS tests. However, since flaring of the flaps in S4 test is restricted this can reduce the contribution. Nevertheless, the contribution of the crack driving force coming from the pressure acting on the flaps in S4 test could be higher, thus for the worst case this *scaling factor* between these two tests could be assumed as 1/0.28 or 3.57.

Thus, the correlation between FS test result and S4 test result could be written as:

$$p_c = 3.57 (p_{cS4}) \quad (9.1)$$

When this correlation is applied to the S4 test results from 250 mm SDR 17.6 MDPE pipe at 0 °C, Eq. 9.1 would predict the full scale p_c to be 6.071 bar. This value confirms the p_c results of BG full scale test on the same size MDPE pipe tested at the same temperature, which is 6.1 bar.

For 180 SDR 11 and 17.6 MDPE pipes, Eq. 9.1 would predict the p_c to be 8.04 bar and 7.5 bar at 0 °C; 48.2 bar and unattainable at 15 °C, respectively. Similarly, for 180 SDR 11 TUB 125, the predicted FS test p_c would be 35.7 bar at -15 °C, 51.8 bar at -10 °C and unattainable at 0 °C.

Thus, the inevitable cost of reducing the length scale of the test by suppressing axial decompression has been to reduce the critical pressure, but by a physically explicable scale factor.

9.3. Correlation Between the Charpy Impact and S4 Test Results

To relate the material's performance with the other controlling parameters, i.e. pipe dimensions and pressurising medium, to predict the p_c of any plastic piping system requires a vast range of exploration and basic research on fluid dynamics, material dynamic fracture mechanics, and structural mechanics aspects of pipe fracture. Much more powerful models are needed to account for these controlling parameters in a sound design procedure without any empirically free terms.

Bearing this complicated nature of the problem in mind, it was thought that, firstly, it might be of help to correlate the governing parameters to obtain a crack arrest criterion. The second implication of such a correlation is that it could give a design engineer an insight about the variation of p_c with the controlling parameters.

Vanspeybroeck (1990) has compared his instrumented Charpy impact energy results with the Modified Robertson pipe test results on fourteen different PEs at 0 °C. In these pipe test results, the final crack length was used as a criterion for arrest/propagation (*i.e.*, if a single crack ran into the pressurised section and stopped abruptly before propagating 1.5D, the test

conditions were said to be those required to arrest a rapid crack, and propagation was said to occur if the crack ran further than 3D). From his impact energy versus final crack length comparison it was concluded that the Charpy impact energy is an unreliable parameter in predicting RCP performance.

More recently, Marshall & Harry (1990) have attempted to correlate Charpy impact and FS pipe test results. It was pointed out that the Charpy impact energy results and the 'modified strain energy theory' might be used to predict p_c . In their correlation it was assumed that not the total but only 26% of the strain energy is used to propagate the crack. It has to be pointed out that this correlation was obtained from only one temperature (0 °C). Thus, it is not yet known whether it is also valid for a wide range of temperatures.

The variation of the Charpy impact results, presented in Chapter 4, with test temperature shows a wide variation with respect to the way the results were analysed. Using the analysing technique detailed below, it seems that the variation has a more or less similar temperature dependence as in the pipe results:

- Un-side grooved Charpy specimens have to be used.
- The total amount of energy consumed during the whole fracture event has to be taken into account in the analysis rather than the energy dissipation up to the time at which the maximum force is reached.

Despite this agreement the rate of increase in p_{CS4} test results with temperature is relatively higher than the rate in the Charpy impact energy results. On the basis of present extensive S4 test results on various pipe dimensions of BP 00-40 and TUB 125 PEs presented in Chapter 6, and elastic modulus results from the ultrasonic technique and the Charpy impact results on the same grades presented in Chapter 4, the following empirical correlation was obtained from a non-linear curve fitting technique for prediction of p_{CS4} value;

$$p_{CS4} = 0.378 E^{-0.901} (C_v)^{2.457} \frac{t}{D^{0.78}} \quad (9.2)$$

where,

- p_{cS4} = S4 critical pressure (bar)
- C_v = Total Charpy impact energy (kJm^{-2})
- t = Pipe wall thickness (mm)
- D = Nominal pipe outside diameter (m)
- E = Elastic modulus (MPa)

On the bases of available present data base, using the above empirical equation, the p_{cS4} values could be predicted to an accuracy of $\pm 78\%$. Additionally, one should bear in mind that the total Charpy impact energy considered in this equation for p_{cS4} is the value that should be obtained from the test circumstances and specimen sizes that were detailed in Chapter 4. Moreover, it has to be recalled that more extensive results on wider dimensions and different grades are required to check and, if necessary, to improve this empirical correlation. The above equation could give a rough-and-ready correlation between the Charpy impact and measured p_{cS4} test results if the pipe wall thickness and crack velocity are above the so called critical thickness and/or the minimum crack velocity, respectively. To conclude, it becomes apparent that although the Charpy impact tests are informative in classification of the materials, any design procedure based on such test results is not safe to rely on and could be rather misleading.

9.4. Relationship Between the HSDT and S4 Tests: an Assessment

Employing the HSDT technique Wheel (1991) carried out a series of experiments on BP 002-40 and TUB 125 at 0°C to characterise their dynamic fracture fracture resistances for a wide range of crack velocities. His results on the two grades indicated that crack resistance remained constant over the crack velocity range 100 to 200 ms^{-1} , showing mean crack resistance values as 3.0 kJm^{-2} and 5.0 kJm^{-2} for BP 002-40 and TUB 125, respectively.

As far as crack velocity is concerned there is a minimum velocity below which RCP is not sustainable. The velocity measurements in HSDT tests shows a cut-off velocity of about 150 ms^{-1} for BP 002-40 and 125 ms^{-1} for TUB 125 at 0°C . For lower propagation velocities fracture surfaces showed a number of crack arrest lines. Similarly, as far as RCP in PE pipes

is concerned, crack velocity measurements on step-tapered as well as uniform thickness pipes indicated that there is a well defined minimum crack velocity of about 70 ms^{-1} for BP 00-40 at 0°C . Although the crack resistance of pipe grade PEs at slower crack velocities is known to be higher, the lack of crack resistance values for lower crack velocities means that the exact variation is not yet known.

As far as currently available dynamic tests are concerned, the HSDT test seems to be the most encouraging one, with a similar deformation and thus crack driving mechanism as RCP in PE pipes. Crack front shapes in both tests are very similar to each other. Additionally, the SEM fracture surface comparison indicates that those of HSDT specimens have the same microscopical features as seen in the region between the inner and outer surfaces of the pipe fracture surfaces. The most striking distinction is the tear-lips on the pipe fracture surface, a narrow region which has undergone extensive tearing type plastic deformation at each side of the sample, particularly at the inner surface. The experiments on inner surface slit pipes clearly showed that having a shallow slit eliminated not only the tear lips at the inner side but also removed the ductility occurring close to it and generated clean, quasi-brittle fracture surfaces as seen in HSDT tests. Although no crack velocity measurements were undertaken it is believed that crack velocity in these slit pipe experiments is lot higher than in normal tests and could be the same as in the HSDT tests. The long crack front shape seen in the HSDT test and the slit on each side of the HSDT sample along the prospective crack path, increase the effective thickness to the extent that could yield the thickness independent plane-strain fracture resistance value.

The S4 test results show that there is strong interaction between pipe wall thickness, crack velocity, and crack driving force. More obviously, the fracture event taking place in PE pipes could not be assumed to be a plane-strain fracture. However, as far as the current bases of fracture mechanics principles are concerned, it is hard to classify the degree of plane-stress, or to define how a plane-strain fracture toughness value could be used or modified in order to

analyse plane-stress failures. As long as this problem remains unsolved, one needs the actual value of the fracture resistance for a particular thickness at particular temperature to start to analyse the problem.

S4 test results on inner surface slit TUB 125 and BP 002-40 180 mm SDR 11 pipes support the HSDT test results in that the p_{cS4} test results for TUB 125 at room temperature are only about 50% higher, which is in agreement with HSDT test results at 0 °C. Although, at present, no HSDT results are available on both materials at room temperature, the p_{cS4} test results on inner surface slit pipe indicated that p_{cS4} did not vary significantly with temperature (see Fig. 6.30).

Critical pressure definition tests on inside grooved pipes show that the worst scenario could be a shallow slit along the pipe's axial direction. This might be introduced during the pipe extrusion process or service operation by some means (such as any scratch created by solid particles in the transported media). For such circumstances the HSDT test results have to be considered in the pipeline's design stage.

THE MAIN CONCLUSIONS AND RECOMMENDATIONS

In the present work, various aspects of phenomenon of RCP in gas pressurised PE pipes were studied extensively during development of the Small Scale Steady State (S4) test technique, developed during this course. The research involved the investigation of a wide range of parameters affecting and controlling the aspects of crack initiation, steady state crack propagation, and arrest as well as a better understanding of the RCP process.

A series of experiments was carried out on BP 002-40 and Aldyl 'A' 180 SDR 17.6 PE pipes to measure the residual stress profile through the thickness. The results were analysed using various available techniques, and indicated that residual stresses are compressive at the outer section and tensile at the inner section. The tensile stress at the inner surface generally ranged between 0.8 - 1.1 MPa while the compressive stress at the outer surface ranged between 1.8 - 2.8 MPa.

The main objectives have been to develop a small scale test technique that could be used to define the maximum allowable operating pressure of a given pipeline system at a given temperature. The main features of the technique could be summarised as follow: a fast crack is initiated by a radial impact from a fired projectile onto a well supported initiation zone. This artificially initiated crack, at a velocity of up to 700 ms^{-1} , is then injected into a propagation zone along which internal gas flow baffles and an external containment cage against flaring suppress decompression and control the crack tip environment. For pressures higher than the critical value (p_{cS4}) the crack velocity settled to a constant value or, for test pressures lower than the p_{cS4} , the crack velocity rapidly fell to zero.

Critical pressure definition tests were carried out on 180 mm and 250 mm SDR 11 and 17.6 MDPE and HDPE pipes at a wide range of temperatures, and the variation of final crack length with test pressure was measured. All the test results indicated a well defined *critical pressure* (named p_{cS4}) at which a sharp transition from crack arrest to complete propagation occurred.

The crack velocity measurements for test pressures above some critical value indicated that suppressing decompression of the pressurising fluid by restraining the flaring of the pipe flaps behind the crack tip are effective strategies for promoting a steady state crack propagation velocity within a small-scale test and thus, for designing a representative Small Scale Steady State (S4) RCP test for pipe.

Results from volume reduced pipe experiments indicated that the prediction of p_c from the Irwin-Corton equation is not straightforward, in that the amount of the energy coming from the strain energy stored in the pipe is only about 15% of the total energy consumed during the fracture process. Moreover, this ratio (15%) is not constant but for faster propagating cracks it is expected to be lower.

Despite RCP being sustained at a much lower critical pressure, the parameters influencing RCP exhibit a well defined effect in good agreement with the results obtained from full-scale tests. On the basis of gas dynamic concepts of RCP in gas pipelines, a good correlation was established between full-scale and S4 tests: p_{cS4} results are only 28% of the actual FS test results for the same dimensions and test temperature.

From extensive instrumented pipe experiments, it was clearly seen that there is a *maximum* as well as *minimum* crack velocity for a steadily propagating crack. The maximum recorded steady state crack velocity was found to be 375 ms^{-1} for 180 SDR 17.6 BP 00-40 pipe tested at -15°C which is in accord with a theoretically estimated value. Similarly, instrumented pipe tests on the same material and size showed a minimum sustainable crack velocity which was about 140 ms^{-1} at -15°C and 70 ms^{-1} at 0°C .

As far as pipe wall thickness is concerned, a large number of experiments has indicated almost linearly increasing critical pressure with thickness but this rate of increase is relatively small compared to the increase in the thickness (about 1/10), and only while the thickness at this temperature exceeds some value. An important outcome of the present research is that there is a *minimum critical thickness* below which RCP is not sustainable for any test pressure. It is also well established that the value of this critical thickness increases with

temperature and varies from one material to another, and may serve as a useful additional index of pipe RCP performance. This thickness effect is attributed to the fact that, as shown by examination of fracture surfaces by SEM, the amount of ductility on the surface is increased by decreasing thickness as well as by decreasing crack velocity. Using the S4 technique, for a given temperature an effective way of defining this critical thickness was developed: *step-tapered* pipe testing. Additionally, this step-tapered pipe technique was used to record crack history along the different thickness pipe sections. These results revealed that the the steady-state crack velocity increases linearly with increasing the thickness.

A fractographic study aimed to gain better understanding of RCP in PE highlighted that the fracture surface is not uniform through the thickness but that *tear lips* at each side, particularly on the inner surface, dominate the performance. Between these two highly stretched sections, there seems to be a gradual decrease in ductility from the inner to the outer section. For a given material tested at a particular temperature, the size of the tear lips at the inner surface remained unchanged with varying thickness but the size of these tear lips at the outer section increased with decreasing the pipe wall thickness. Experimental results on inner surface slit pipes indicate that eliminating the tear lips, particularly at the inner side, enormously decreased p_{CS4} , and resulted in RCP at a pressure of 1.5 bar at room temperature. These tests also indicated that p_{CS4} for TUB 125 HDPE is only about 50% higher than for BP 002-40 MDPE, which is in agreement with the dynamic plain-strain crack resistance data obtained from the HSDT technique.

Across a wide range of temperatures, a series of instrumented Charpy impact tests was conducted on both MDPE and HDPE materials used in critical pressure definition tests. Comparison of the results illustrated that design procedures based on Charpy data are unreliable and non-conservative.

In order to gain an insight into the deformation and stress patterns of RCP in gas pipelines, the problem was analysed quasi-statically by Pafec, a commercial FE code. The main conclusions of this investigation could be summarised as follows: for the considered test conditions, the average axial stress through the thickness around the crack tip is about 65%

higher than the circumferential stress. Secondly, the axial and circumferential stress states through the thickness are not constant but take their maximum values at the inner pipe surface: about 6 times higher than on the outer side.

RCP propagation in PE gas pipelines was also investigated by a special dynamic FE code developed by SwRI in the USA. After obtaining a series of numerical results from the original program by which RCP in FS pipe has been studied, it was modified to analyse S4 tests. The comparison of results from these two versions of the FE code has more or less justified the validity of the correlation between FS and S4 test results. The stress state around the propagating crack was found to be similar to but slightly lower than that seen from quasi-static analysis (this was attributed to inertial effects).

A simple cantilever beam model was proposed to explain the existence of high axial stresses around the crack tip. It was also concluded that it is this high axial stress caused crack path instability and branching. The experiments on quasi-statically loaded PMMA pipes show that the crack can deviate from its initial direction when the ratio of axial stress to hoop stress exceeds unity. Then it was suggested that the creation of any stress state at the crack tip takes some time but as and when it is done, the position of it along the prospective axial crack propagation direction does not change as the crack deviates.

Because of the nature of RCP in gas pipelines a tri-axial stress state at the crack tip is created. However, as far as current fracture mechanics principles are concerned, the effect of stress tri-axiality, or even stress bi-axiality upon RCP has not yet been well established. The available literature on this aspect shows the unresolved complexity of the problem (see Radon & Leever, 1977 and Eftis *et al.*, 1990). As a result, it is not known very well the influence of multi-axial stress state on resistance and response of material to crack propagation.

The variation of yield stresses of PE pipe materials at high strain rate for a wide range of temperatures could be studied using Hopkinson Bar test technique, and these values could be added into Eq. 9.2 so as to predict the critical pressure (may be more accurately).

The inducements responsible from the variation of the ductility seen on the fracture surface through the pipe thickness remain unexplained. It is not known whether it is the residual stresses in the pipe, the crystallinity profile through the thickness, and/or the axial and circumferential stress gradients around the crack tip region. Tests on inside step-tapered pipe could yield valuable information.

With the present version of SwRI's FE program, only uncoupled calculations were conducted. Since in FS tests the pressure history behind a propagating crack is a function of pipe wall thickness as well as the degree of pipe wall restraint (which is not considered in the present uncoupled code), the coupled version of SwRI's FE code has to be used for a better and reliable analysis. Additionally, with the coupled version of the FE code, the pressure decay behind the crack tip in the S4 test (if it exists) could also be modelled, and then for given pipe dimensions and RCP parameters, the interdependence between S4 and FS test results accurately investigated.

The common fracture mechanics specimens are generally side grooved, as in the HSDT test, to promote uniformity of the crack tip stress field, but this may affect the genuine fracture toughness measured because of change in net thickness and constraint. For a reliable procedure to predict the critical pressure, the genuine dynamic crack propagation resistance, rather than the plane-strain fracture toughness, must be evaluated at the required temperature, thickness and velocity values.

As instrumenting the S4 test specimen with timing tracks for crack velocity measurement is time consuming, the test section could be instrumented with high response pressure transducers so as to record the crack velocity history.

Finally, we are pleased to note that the S4 test technique developed during this research is under consideration by the ISO (International Standards Organisation) as a standard type test.

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