

AN EXPERIMENTAL STUDY OF THE DYNAMIC
RESPONSE OF NOTCHED BARS

by

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V

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ABSTRACT

A survey is made of analytic, experimental and numerical techniques in the field of the dynamic initiation, and early propagation, of cracks. As no closed form analytic solutions exist for finite geometries, even in the elastic case, numerical and experimental techniques have to be developed. In instrumented impact tests plasticity often occurs.

The specific problem of the Instrumented Charpy Test is discussed in detail by virtue of its technological significance and the extensive literature available for the test. Although a standard for the test has been proposed there are still outstanding questions to be answered, for which the techniques described above can be used.

The problem of the dynamic calibration of various notched geometries is addressed in the original work of the thesis. The Charpy, Izod, Slender Cantilever and Double Notched Bar geometries are studied using dynamic photoelasticity and 8000 fps photography. It is shown that the response of the DNB is more straightforward than the Charpy geometry.

Further photoelastic study of the latter two geometries, using epoxy model material and 10^6 fps photography, gives a quantitative measure of the growth of stress intensity factor at the notch tips and hence a dynamic calibration is deduced. An explicit finite difference code is used to supplement photoelastic data.

Having achieved progress in the derivation of the dynamic calibration of the two selected geometries, corresponding instrumented

impact tests are then undertaken. The Hopkinson Pressure Bar method of loading is used.

It is concluded that the proposed standard for the Instrumented Charpy Test is valid within limits but that there is a requirement for a dynamic calibration. Such a calibration is complex in the case of the Charpy geometry whereas a simpler geometry, viz. DNB, could prove to be more amenable to analysis and hence be more practical from the technological point of view.

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Notation

a	Flaw depth for edge notched bar.
$\dot{a}$	Crack tip velocity
B	Charpy specimen thickness.
C	Constant for effective mass.
CVNE	Charpy 'V' Notch Energy.
CVN	Charpy 'V' Notch Specimen.
c_1	Velocity of dilatational waves.
c_2	Velocity of distortional waves.
c_0	Velocity of longitudinal or P waves.
C_{LL}	Quasistatic specimen <i>calibration</i> .
f_{σ}	Birefringence Constant.
h	Thickness of 2D Epoxy model.
HPB	Hopkinson Pressure Bar
HV30	Vickers Hardness 30 lb Range.
J	J Integral
K_{Id}	Dynamic Initiation Fracture Toughness
K_{Ic}	Plane Strain Fracture Toughness under Static Loading
K_{Im}	Crack Arrest Toughness assuming dynamic conditions
K_{Ia}	Crack Arrest Toughness assuming quasistatic conditions
$\dot{K}_I$	Rate of change of Mode I stress intensity factor
$K_I(t)$	Time dependent Mode I stress intensity factor.
K_{ID}	Stress Intensity Factor at a propagating crack
$k_I(t)$	Time dependent Mode I dimensionless stress intensity factor.
K_{IQ}	Fracture Toughness
K'	Strain Gauge Factor.
m	Mass of beam
m'	Mass per unit length

M_{p0}	Fully plastic moment.
N	Fringe Order
OUEL	Oxford University Engineering Laboratory
P_m	Maximum load
P_f	Fracture load
PCVN	Pre-cracked 'V' Notched Specimen
$P(t)$	Time dependent load.
R_c	Rockwell C Hardness value.
S	Charpy specimen span
t_f	Time from Specimen being Loaded to Fracture.
T_i	Kinetic Energy of Impacting Weight
T_R	Response of Electronic Instrumentation
v_1	HPB Impact Velocity
v_2	HPB Incident Particle Velocity in Loading Bar
v_3	HPB Particle Velocity in Loading Bar
V'	Strain gauge circuit voltage
V	Weight Impact Velocity
W	Charpy Specimen Depth
W_M	System Energy dissipated to maximum load.
w_1, w_2, w_3	Resonant frequency of simply supported beam.
δ_m	Maximum mid point deflection
$\delta(t)$	Time dependent mid point deflection
ϵ	Strain
λ	Elastic Constant : Lamé's Constant
μ	Elastic Constant : Shear Modulus
σ_1	Principal Stress
σ_2	Principal Stress
$\sigma_{0.2}$	0.2% proof stress

$\sigma_{0.5}$	0.5% proof stress
σ_y	Yield Stress
σ_{nom}	Nominal Stress
ρ'	Radius of Curvature
ρ	Material Density
τ	Period of Apparent oscillation
ϕ	Lamé displacement potential : dilatation
ψ	Lamé displacement potential : distortion

CHAPTER I

1. INTRODUCTION : MATERIAL FAILURE AT ELEVATED STRAIN RATE

The rupture of an engineering material is influenced by 3 environmental factors namely a notch (or constraint), temperature and strain rate^[1]. The presence of a notch gives rise to a triaxial stress state, increasing the hydrostatic stress and hence promoting cleavage^[2]. The lowering of the ambient temperature also promotes cleavage.

If a tensile specimen of stainless steel is loaded at different strain rates so the stress-strain behaviour of the material changes (see figure 1.1, from^[3]). Typically the yield stress increases, the Young's Modulus increases, and the rupture strain decreases for increased strain rate. The rupture becomes more influenced by cleavage as the strain rate increases^[1].

Thus it should be expected that the combined influence of constraint and imposed strain rate should change the fracture toughness of a material, whether defined by a critical stress intensity factor or by an energetic parameter. An idealized variation of K_{Id} , the Dynamic Initiation Fracture Toughness, with loading rate is given in figure 1.2 from^[4]. Note that in the case of a 'mathematical singularity' characterization of a flaw, the strain rate has to be expressed in terms of $\dot{K}_I$ where $\dot{K}_I = \frac{K_{Id}}{t_f}$ (t_f is the time to fracture from initial loading). The various loading regimes have been identified on figure 1.2. The dynamic effects can be categorized into the influence of inertia on the stress distribution about the crack and the variation of material properties, including crack initiation, with strain rate^[16].

The variation of fracture toughness with loading rate has various technological implications. Firstly the critical flaw size in a dynamically loaded structure *may differ from that in a* corresponding structure loaded statically. Difficulties occur in specifying the dynamic field in a complex product and in applying a Dynamic Initiation Fracture Toughness as a material property. Figure 1.3 shows a flawed chain link under dynamic loading. To solve the problem a dynamic calibration would be required and the value of K_{Id} at the specified loading rate for the material applied. Such a problem is currently intractable and so the study of idealized laboratory impact tests advances understanding of the dynamic behaviour of idealized cracked geometries. Such a study has the more important task of specifying in a quantitative manner, the crack initiation behaviour of the material.

In Static Fracture Mechanics the Plane Strain Fracture Toughness, K_{Ic} , can be regarded as a material property as the plastic constraint in the fracture zone is maximum. Due to the embrittling effect of strain rate brittle fracture can occur in specimens under impact loading that would otherwise fail in a ductile manner under quasistatic loading. The interaction between cleavage, tearing and strain rate is complex; hence characterizing material failure in the dynamic case is more complex than in the static case.

The Power Engineering Industry has been interested in dynamic fracture toughness over recent years and have stimulated extensive discussion of various aspects of the subject, see for example [6, 7, 8]. The main motivation has concerned nuclear pressure vessel design [9, 15]. On the otherhand the UK Offshore Engineering Industry is not active in the field as yet - they are currently more interested in the effect of strain rate on the material response in structures as opposed to the influence of strain rate on rupture [10, 11].

Given the above general design requirement for the extraction of Dynamic Initiation Fracture Toughness Values from laboratory tests, the aim of the thesis is to analyse various instrumented impact tests with a view to correlating $K_{I\Delta}$ values and seeing which geometry is the most simple to analyse. The task can be formulated most generally as the analysis and characterization of material damage in the vicinity of a stress concentration under elevated strain rate. The standard approaches of macroscopic fracture mechanics can be used namely the crack resistance or energetic approach and the stress intensity fracture approach^[12]. Another possible line of attack is the scrutiny of microscopic phenomena^[13].

In this thesis the macroscopic approach is used, characterizing the material as an isotropic continuum and using the ideas and concepts of fracture mechanics. The elastodynamic response is concentrated upon but some discussion is given for inelastic phenomena. In many instrumented impact tests of materials of engineering interest inelastic effects predominate.

The tendency in the literature on instrumented impact testing, see for example^[6], is to search for technological solutions to specific problems without gaining fundamental insight and a unified understanding of the underlying problem. Technological solutions may work for a specific state of affairs, viz the proposed ASTM standard^[14], but problems are encountered outside these limits. This technological approach has led to many research man years being expended for only modest progress. The approach used in this thesis is both fundamental and interdisciplinary. The starting point has been taken as the general one of 'The Fracture Initiation Behaviour of Notched Beams and Bars under Impact Loading'. The basis for the concepts and ideas used in Dynamic Fracture Mechanics^{are} discussed

(Chapter 2) and the specific problems associated with the Instrumented Charpy Test highlighted (Chapter 3). The analytic, experimental and numerical techniques discussed are then brought to bear on various notched geometries in the original work of the thesis. (Chapters 4, 5, 6). The results are finally put into the context of the initially posed, general problem. (Chapter 7).

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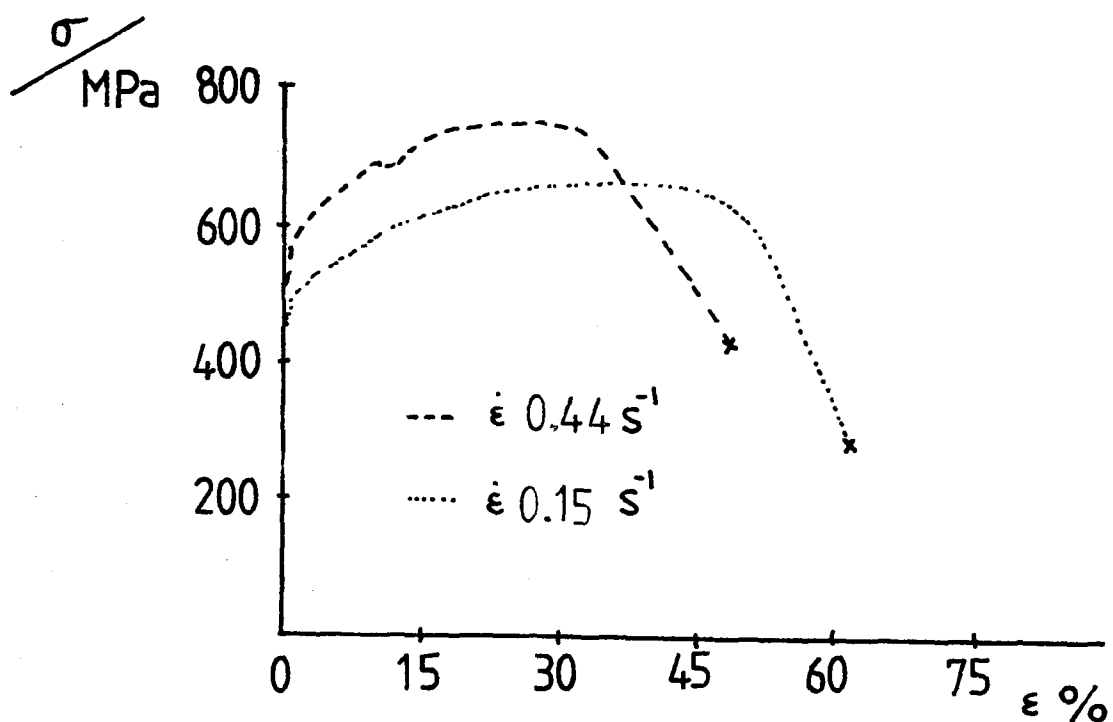


Fig. 1.1 Stress-Strain curves at various strain rates for steel.

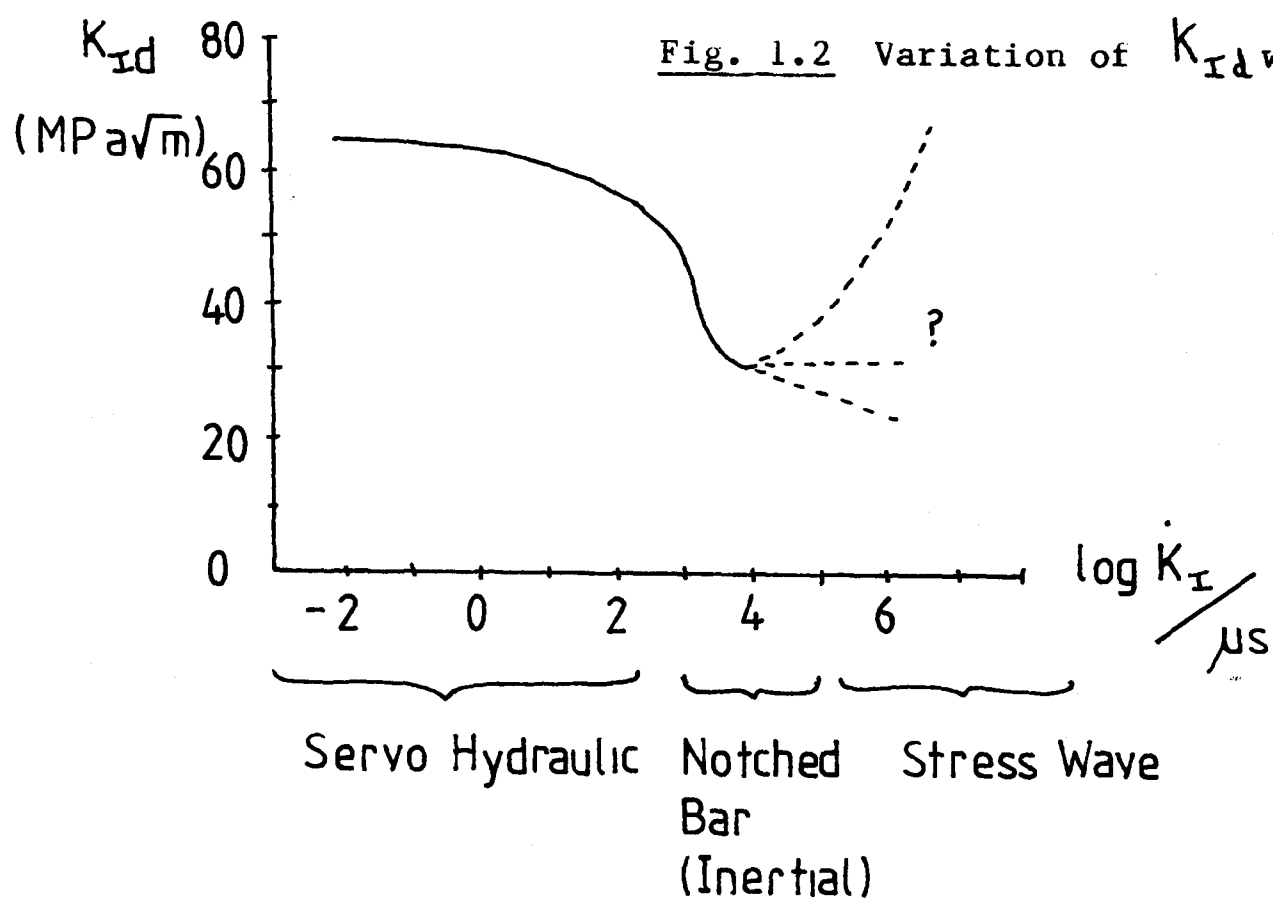


Fig. 1.2 Variation of K_{Id} with strain rate.

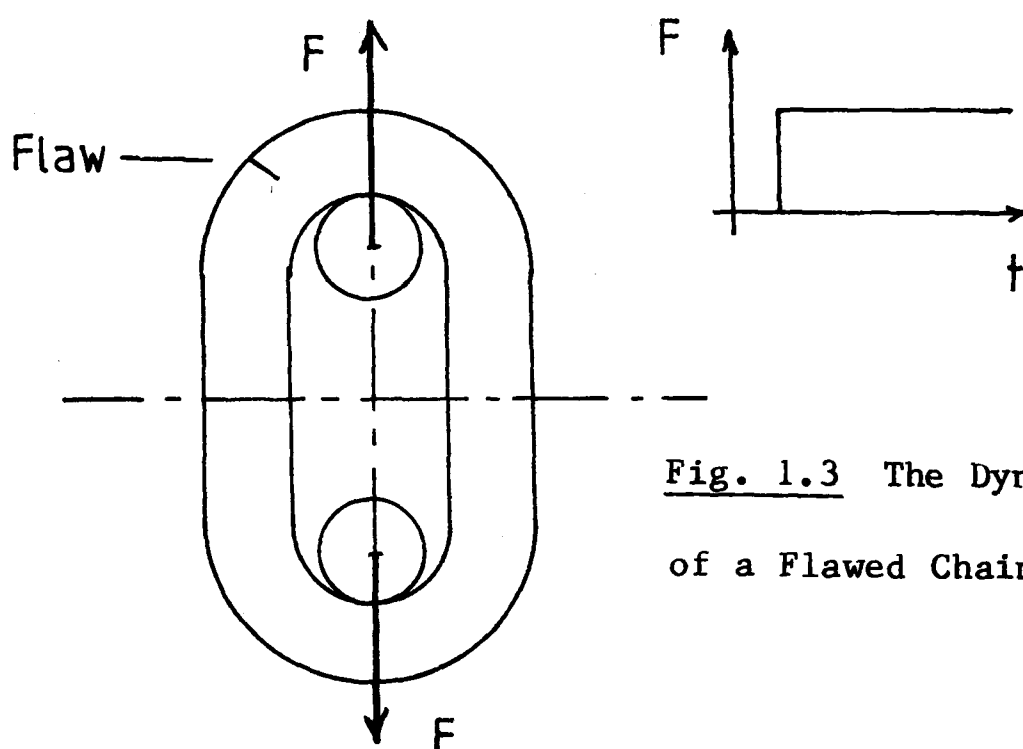


Fig. 1.3 The Dynamic Fracture of a Flawed Chain Link

2. An Introduction to Flaw Initiation under Dynamic Loading

2.1 Introduction

The problem posed is the analysis and characterization of material damage in the vicinity of a stress concentration under conditions of elevated strain rate. The approach used is macroscopic continuum mechanics. Initial discussion will centre around the idealized condition of linear elastic behaviour for analyzable cracked geometries. The survey will then embrace more practical problems viz:-

1. Notched, as well as cracked, geometries.
2. Finite Geometries.
3. Inelastic Behaviour.
4. Crack propagation and arrest as well as crack initiation.

Thus the survey will finally focus on the specific problems associated with Dynamic Initiation Fracture Toughness Testing.

2.2 The Elastodynamic Response of Bodies without Flaws.

2.2.1 Elasticity Solutions

The linear elastic isotropic response of a material under dynamic loading can be modelled mathematically as follows:-

1. Equations of motion

$$\frac{\partial \sigma_x}{\partial x} + \frac{\partial \tau_{xy}}{\partial y} = \rho \frac{\partial^2 u}{\partial t^2} \quad 2.1$$

$$\frac{\partial \sigma_y}{\partial y} + \frac{\partial \tau_{xy}}{\partial x} = \rho \frac{\partial^2 v}{\partial t^2}$$

where u, v are displacements in the x, y directions

2. Stress - Strain

$$\begin{aligned}
 \sigma_x &= \lambda \left(\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} \right) + 2\mu \frac{\partial u}{\partial x} \\
 \sigma_y &= \lambda \left(\frac{\partial u}{\partial x} + \frac{\partial v}{\partial y} \right) + 2\mu \frac{\partial v}{\partial y} \\
 \tau_{xy} &= \mu \left(\frac{\partial v}{\partial x} + \frac{\partial u}{\partial y} \right)
 \end{aligned} \tag{2.2}$$

3. Lamé Displacement Potentials ϕ, ψ

$$\begin{aligned}
 u &= \left(\frac{\partial \phi}{\partial x} + \frac{\partial \psi}{\partial y} \right) \\
 v &= \left(\frac{\partial \phi}{\partial y} - \frac{\partial \psi}{\partial x} \right)
 \end{aligned} \tag{2.3}$$

Combining 2.2, 2.3, gives:-

$$\begin{aligned}
 \sigma_x &= \lambda \nabla^2 \phi + 2\mu \left(\frac{\partial^2 \phi}{\partial x^2} + \frac{\partial^2 \phi}{\partial x \partial y} \right) \\
 \sigma_y &= \lambda \nabla^2 \phi + 2\mu \left(\frac{\partial^2 \phi}{\partial x^2} - \frac{\partial^2 \psi}{\partial x \partial y} \right) \\
 \tau_{xy} &= \mu \left(\frac{2 \partial^2 \phi}{\partial x \partial y} - \frac{\partial^2 \psi}{\partial x^2} + \frac{\partial^2 \psi}{\partial y^2} \right)
 \end{aligned} \tag{2.4}$$

Combine 2.1, 2.3 and 2.4 to derive the wave equation satisfied by ϕ, ψ :-

$$\begin{aligned}
 \frac{\partial}{\partial x} \left[(\lambda + 2\mu) \nabla^2 \phi - \rho \frac{\partial^2 \phi}{\partial t^2} \right] &= \frac{\partial}{\partial y} \left[\rho \frac{\partial^2 \psi}{\partial t^2} - \mu \nabla^2 \psi \right] \\
 \frac{\partial}{\partial y} \left[(\lambda + 2\mu) \nabla^2 \phi - \rho \frac{\partial^2 \phi}{\partial t^2} \right] &= - \frac{\partial}{\partial x} \left[\rho \frac{\partial^2 \psi}{\partial t^2} - \mu \nabla^2 \psi \right]
 \end{aligned}$$

which are satisfied if:-

$$(\lambda + 2\mu) \nabla^2 \phi = \rho \frac{\partial^2 \phi}{\partial t^2} \quad 2.5$$

$$\mu \nabla^2 \psi = \rho \frac{\partial^2 \psi}{\partial t^2} \quad 2.6$$

Equation 2.5 is a wave equation for dilatational waves which travel at a speed given by $c_1 = \sqrt{\frac{\lambda + 2\mu}{\rho}}$, and equation 2.6 is for shear waves which travel at a speed of $c_2 = \sqrt{\frac{\mu}{\rho}}$. Thus when a disturbance travels through an infinite solid the deformation of the solid can be split into two components - one associated with hydrostatic deformation and the other associated with distortion. Typical values of C_1 and C_2 for engineering materials are given in table 2.1.

Consider next the occurrence of a free surface e.g. an infinite half space. In this case the dilatational and shear waves interact with the boundary in a complex manner^[1]. Another effect of the free surface is to trap surface waves near the boundary, the behaviour of these waves being analogous to gravity water waves i.e. the disturbance decays as one travels into the material. These waves are associated with Lord Rayleigh and have a distinct velocity, C_R , associated with them (see table 2.1), which is independent of frequency^[2].

The solution of any extended media problem lies in the mathematical characterization in terms of potential functions ϕ and ψ , specification of boundary conditions and initial conditions. Solution techniques depend on the geometry and loading under consideration viz Fourier transforms for Harmonic Loading, Laplace transforms for Transient Loading, Hankel transforms for axisymmetric problems etc^[3]. As the geometry under consideration becomes more

complex so analytic solutions fast become intractable.

2.2.2 Strength of Materials Solutions

In the 'simple' case of a stress pulse travelling down a cylindrical rod or bar a full elasticity solution is very complex. The Pochhammer and Chree type solutions are unable to admit realistic end conditions for stress and displacement and give rise to complex frequency equations^[4]. Thus a simplified, strength of materials, approach is required specific to a one dimensional pulse travelling along a rod.

In such a theory, described in^[4], the effects of transverse strain and inertia are neglected as are also gravitational and all dissipative forces i.e. damping. Also it is assumed that the pulse length is at least six times the crosssectional dimension of the bar. This theory illustrates many aspects of stress wave behaviour and is of importance in the analysis of the phenomena associated with the Hopkinson Pressure Bar apparatus, which is used in the experimental work of the thesis.

From equilibrium and stress-strain relations:-

$$\frac{\partial^2 u}{\partial t^2} = \frac{E}{\rho} \frac{\partial^2 u}{\partial x^2} \quad 2.7$$

(where u is the displacement in the x direction)

i.e. a wave equation whose waves are characterized by the velocity

$$c_0 = \sqrt{\frac{E}{\rho}} \quad \text{Typical values are given in table 2.1}$$

From strain considerations the intensity of stress propagated is given by:-

$$\sigma = \rho c_0 v \quad 2.8$$

where v is the particle velocity. Given these basic equations we can now analyse the behaviour of a pulse in a single rod or collection of rods. Appendix B discusses a rod problem associated with the photoelastic work of the thesis, in which the phenomena of transmission, reflection and particle velocity are discussed.

2.3 The Elastodynamic Response of Bodies with Flaws

2.3.1 Strength of Materials Solutions

Consider next a cylindrical rod with a circular groove^[5]. A tensile pulse, in which the particle velocity is in the opposite direction to pulse propagation, is incident on the notched section (see Figure 2.1). When the pulse reaches the notch it is reflected as compression at the free surface whilst being transmitted as tension in the central ligament. The result is a discontinuity in particle velocity at the notch tip causing the tip to act as a secondary wave source. The simple one dimensional wave theory breaks down.

Dilatational, shear and head waves will emanate from the notch tip with their characteristic velocities (see figure 2.2). The head wave is a compound of dilatation and shear, joining the two types of waves. Thus the stress state is now a complex interaction between the incident pulse, the secondary diffraction and, later, reflections from the free surface. After large time, after many reflections, a quasistatic stress distribution may result. On the other hand the stress levels may increase until fracture occurs.

Such an analysis is relevant from a qualitative point of view but the mathematical characterization is problematic and more idealized geometries need to be considered if an analytic solution is to be found.

2.3.2 Elasticity Solutions

2.3.2.1 The Stationary Crack

Consider the diffraction of a pulse about a semi infinite crack in an infinite medium. The initial solution shown in figure 2.2, prior to the boundary taking effect, is applicable. The type of mathematical analysis required is well illustrated by the description of the Sommerfeld diffraction problem given in [1,6], which looks at the diffraction of acoustic waves only and hence means that only a single, scalar, wave potential ϕ needs to be considered. Also a harmonic disturbance is considered.

In the method the wave equation is solved subject to continuity in ϕ , bounds on ϕ and to edge conditions. In order to cope with the semi infinite boundary condition of the crack a similar procedure is adopted as with elastostatic crack problems [7] viz Fourier transforms, Analytic functions and analytic continuation arguments. The problem is reduced to the standard Wiener - Hopf equations [1], which are solved by the application of Liouville's theorem. After Fourier inversion the total field potential function ϕ can be expressed in terms of an integral that can be solved in terms of Fresnel integrals. The result is shown in figure 2.3.

Such a complex solution refers to the simplest possible case. Additional complications are due to shear, necessitating use of function ψ , to free surfaces, to finite cracks, to finite thickness and to transient loading. Thus it can well be seen that the solution for even the most simple geometries can often be intractable. A survey of stationary crack problems that can be currently solved analytically is given in [8].

Given the potential function solution the stress field can be derived directly from equation 2.4, given above. For a singular field the stress distribution in the vicinity of a dynamically loaded

crack is the same as in the static case [8, 9, 10] i.e. for Mode I opening (see figure 2.4) neglecting the far field:-

$$\sigma_{xx}(t) = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left[1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right] + \dots$$

$$\sigma_{yy}(t) = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left[1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right] + \dots$$

2.9

$$\sigma_{zz}(t) = \frac{K_I(t)}{\sqrt{2\pi r}} 2\nu \cos \frac{\theta}{2} + \dots$$

$$\tau_{xy}(t) = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \sin \frac{\theta}{2} \sin \frac{3\theta}{2} + \dots$$

where $K_I(t)$ is the dynamic stress intensity factor. The above has implications for the data reduction methods used in the experimental and numerical work described in this thesis - in the case of a singularity dominated field static data reduction methods may be used. This may not be the case for a notch with a finite tip radius. Note that the singular field is different from that of a travelling crack (see below) and this would suggest that it may well be difficult to connect fracture parameters between initiation and propagation [9].

The above discussion is only for the linear elastic case. Brief discussion will be given for inelastic conditions in paragraph 2.6. Given that for complicated elastic and for most non linear problems no closed form analytic solutions exist then experimental and numerical methods need to be resorted to (see below).

2.3.2.2 Crack Initiation

Having described the dynamic stress field in the vicinity of a stationary crack, the next step is to look at the criteria for crack

initiation. As with static fracture mechanics the two distinct, but related, approaches of crack resistance or energy and stress intensity can be considered^[11].

In the energetic approach elastic strain and kinetic energy in the body is converted to free surface energy as the crack advances. From the displacement field around a crack tip the energy field can be given. But for a non moving crack the energy flux into the tip is zero and this precludes the use of an energy balance for crack initiation^[9].

Thus the stress intensity factor approach is required. The stress distribution around a stationary crack tip has been given in paragraph 2.3.2.1. Of course some yielding has to occur in the vicinity of the crack tip to relieve the infinite stresses, as in the static case, but the plastic zone is assumed to be limited and the LEFM approach assumed to be valid. The crack initiation is described by the attaining of a critical value. Nilsson states^[9] that many 'crack initiation' measurements may be confused with 'early propagation' measurements.

The stress intensity factor should therefore be calculated or measured at the point of crack initiation, and the parameter obtained for various loading rates. Calculations can only be made for extended media and experimental or numerical methods have to be used for practical geometries.

Recently it has been questioned as to whether $K_I = K_{Id}$ is a valid criterion for crack initiation under elastic conditions^[9]. Kalthoff^[52] has shown that there is an incubation time during which the stress intensity factor has to exceed a critical value before crack initiation occurs. This incubation time may be a material property.

These, and other questions, still need to be resolved and further experiments need to be performed. In this thesis the $K_I = K_{Id}$ criterion has been considered to be valid, the above reservations notwithstanding, for the intermediate strain rates associated with notched bar testing. Focussing on microscopic failure criteria^[12,13] highlights the time dependent effects during crack initiation.

2.3.2.3 A Mathematical Crack or a Notch?

The discussion so far has centred around the characterization of the crack as a mathematical singularity. Dynamic solutions for a notch are more complex - potential functions have to be used to describe the stress distribution around the finite notch. No work on this has been found. The stress analysis of notch problems under static loading is described in^[14]. In the situations described in this thesis, see for example the experimental and numerical work, it is assumed that the notch can be reduced to a singularity for most practical purposes.

2.3.2.4. Crack Propagation and Arrest

Although crack initiation is concentrated upon in this thesis, it should be put into context of the entire initiation - propagation - arrest event. In the case of the Instrumented Charpy Test any global energy analysis for the entire fracture event, e.g. CVNE, includes crack propagation and possibly crack arrest effects. Some researchers in the field of Dynamic Fracture believe that crack arrest is the reverse, in time, of crack initiation and that both arrest and initiation are characterizable by a single material property e.g. K_{Ia} and K_{Id} [15]. (See paragraph 3.6.4). Figure 2.5 shows the empirical relationships between K_{IC} , K_{Id} , K_{Im} and K_{Ia} for a Mn - Ni Steel [16].

Elasticity solutions and fracture parameters can be derived for the moving and arresting crack. These aspects are well reviewed in [17, 18, 19, 20]. One of the geometries that is important in regard to crack arrest is the Double Cantilever Beam specimen (DCB). The behaviour of this geometry has been analyzed extensively [18, 19, 20]. Strength of Materials approaches have been used in the analysis of the specimen, but not all dynamic effects are taken into account. Note that crack arrest offers an

alternative in design to crack initiation - it is often less conservative to guard against the former [21, 22].

The crack tip stress field about a propagating crack tip can be described mathematically as follows, for Mode I and σ_{yy} only [5, 18]:-

$$\sigma_{yy}(t) = \frac{K(t)}{\sqrt{2\pi}} B(\dot{a}) \left[- (1 + \alpha_2^2) \frac{\cos\left(\frac{\theta_1}{2}\right)}{r_1^{\frac{1}{2}}} + \frac{4\alpha_1\alpha_2}{(1 + \alpha_2^2)} \frac{\cos\left(\frac{\theta_2}{2}\right)}{r_2^{\frac{1}{2}}} \right]$$

where $\alpha_1 = \left(1 - \frac{\dot{a}^2}{c_1^2}\right)^{\frac{1}{2}}$

$$\alpha_2 = \left(1 - \frac{\dot{a}^2}{c_2^2}\right)^{\frac{1}{2}}$$

2.10

$$r_1 \exp(i\theta_1) = \xi + i\alpha_1\eta$$

$$r_2 \exp(i\theta_2) = \xi + i\alpha_2\eta$$

$$B(\dot{a}) = \frac{(1 + \alpha_2^2)}{[4\alpha_2\alpha_1 - (1 + \alpha_2^2)^2]}$$

and the coordinate system is as given in figure 2.6, $r_1, \theta_1, r_2, \theta_2$ are transformations of r, θ values due to the moving coordinate system and for the purposes of the solution. This variation of $\sigma_{yy}(t)$ should be compared with the case for the stationary crack. (See paragraph 2.3.2.1). As the crack tip velocity, $\dot{a} \rightarrow 0$ so $B(\dot{a}) \rightarrow \infty$ and the square brackets tend to zero. The stress field reduces to the static one.

Limiting

behaviour also occurs when $\dot{a} \rightarrow C_R$: in this case $K(t) \rightarrow 0$ in order that the strain energy release rate remains finite [5].

From the stress field we can derive the displacement and energy fields. For plane strain conditions, the energy flow, F , into the crack tip is given by:-

$$F = \frac{1-\nu^2}{E} \frac{\dot{a}^3 \alpha_1 K_I^2}{(1-\nu) c_2^2 [4\alpha_1 \alpha_2 - (1+\alpha_2^2)]} \quad 2.11$$

where the energy flux is calculated travelling across a contour enclosing the crack tip (see figure 2.6) and shrunk onto it. The energy flux F is related to the dynamic energy release rate:-

$$F = \dot{a} G \quad 2.12$$

For crack propagation, the resistance to fracture, Γ , has to be overcome and the energy balance criterion is given by:-

$$G = \Gamma \quad 2.13$$

The crack resistance is assumed to be only dependent on crack velocity, and the variations of K_{ID} for an epoxy and steel are given in figure 2.8[18, 27]. Kalthoff[52] distinguishes between crack resistance, $\Gamma(\dot{a})$, and the dynamic stress intensity factor, $K_{ID}(\dot{a})$. He discusses whether equations 2.11 - 2.13 represent a valid criterion for crack growth and whether K_{ID} and $\Gamma(\dot{a})$ are unique material properties. K_{ID} is measured experimentally using caustics or photoelasticity. $\Gamma(\dot{a})$ can be derived using an energy balance. Freund[23, 24, 25, 26] has given a complete analysis of the crack propagation problem for a semi-infinite crack in an infinite two dimensional geometry. His analysis shows the complex mechanical nature of the motion of a crack tip. (See also[6]).

The above survey develops the analytic tools and fracture parameters required in the analysis of crack initiation under dynamic conditions. The solutions of practical boundary value problems for finite geometries are intractable and so experimental and numerical methods need to be used.

2.4 The Effect of Finite Geometry and the Derivation of $K_I(t)$

2.4.1 Introduction

It has already been mentioned that in the case of the Double Cantilever Beam specimen a strength of materials analysis is possible. Other 'hybrid' solutions have been proposed for the Charpy geometry, e.g. [28]. Such a solution uses the standard transform techniques but certain assumptions have to be made e.g. beam field equations are solved only approximately and the crack length and time relation has to be specified in advance. Such an approach does, however, give physical insights into specific phenomena e.g. the importance of inertial effects in initiation.

2.4.2 Experimental Techniques

2.4.2.1 The Method of Caustics

When a two dimensional notched geometry is stressed the high stress in the vicinity of the notch gives rise to out of plane deformations, dependent on the thickness of the geometry. Thus if parallel light is incident on this area it is reflected, in the case of an opaque material, or refracted, in the case of a transparent material. The resultant scattered light, when imaged onto a plane a distance z from the specimen, reveals a shadow spot [16]. For the transparent material the refractive index also changes near the notch tip. It can be shown [29] that the diameter of the caustic is uniquely related to the stress intensity factor:-

$$K_I = \frac{2\sqrt{2\pi}}{3m \int^{\frac{5}{2}} |c| dz} D^{\frac{5}{2}} \quad 2.14$$

where variables are defined in figure 2.7.

Thus the stress intensity factor for any plane geometry can be derived experimentally. Any material can be studied e.g. opaque metals or transparent epoxy models. The experimental technique though is complex, requiring accurate alignments and high quality optics. Matters are complicated for dynamic work - typically a multiple spark camera is required capable of firing 24 sparks (say) at preset intervals capable of resolution down to $1\mu s$. In the case of a stationary crack in a dynamic field a static data reduction can be used whereas for a travelling crack a velocity term has to be introduced.

The method has the advantage of giving a direct unequivocal measure of stress intensity factor, and is the most applicable experimental method to date for dynamic fracture problems. The method has been used for the study of DCB and Charpy Specimens. The technique can be adapted for limited plasticity[30].

2.4.2.2 The Method of Photoelasticity

The technique for static photoelasticity has been used for many years[31]. In the method, light is circularly polarized - using a polarizer and quarter wave plate - and is incident on a stressed transparent model. The model material is such that its refractive index changes with stress level. Thus the polarized light is refracted or retarded and the resultant interference patterns are analyzed using another quarter wave plate and analyzer. The resulting fringes, projected onto a screen or camera back, can be related to the principle stress difference in the material. These are termed Isochromatic fringes. Morton and Ruiz[10] have shown

that the fringe loops in the vicinity of a stationary crack can be related to the stress intensity factor at the crack tip using a 'linear slope method'. In fact extensive study has been made of the reduction of fringe loops to fracture parameters for travelling cracks and an extensive number of methods exist[32].

In the case of dynamic fracture mechanics static reduction methods can be used for a stationary crack but velocity parameters have to be included for running cracks. The advantage of the technique is that whole field data can be obtained but difficulties are encountered in scaling from model, e.g. epoxy, material to the prototype, e.g. metal, material. Typically, fringes in dynamic work are caught using a multiple spark camera[33].

This technique is used extensively in the thesis and the reader is referred to the relevant chapters for further details.

2.4.2.3 Other Techniques

A technique that offers potential for dynamic work is the interference optical technique. In this method the deformation in the surface of the model can be derived from interference patterns. It has the advantages of being applicable for opaque materials and for giving whole field data[34].

There is also the general experimental stress analysis method of monitoring strains using strain gauges. This will be discussed in the experimental work of the thesis.

2.4.3 Numerical Techniques

2.4.3.1 Introduction

With the fast development of computing capacity the numerical approach is becoming a major analysis tool at the disposal of the mechanician. The numerical simulation of impact phenomena is discussed extensively in[35, 36]. The well established technique of Finite Differences was used initially, but now Finite Element

Methods dominate drawing on their extensive use in other mechanics fields. More recently the Boundary Element Method has been developed for the analysis of fracture problems.

2.4.3.2 Finite Difference and Finite Element Methods

The major finite difference package available for dynamic fracture problems is HEMP 2D and 3D[35, 38]. The program can be used for both elastic and elasto-plastic cases. In the former an extrapolation scheme is required at the notch tip to overcome the large variations in stress in the tip region. (See Appendix A). In the latter, material failure criteria have to be incorporated into the computer model (see paragraph 3.7). Appendix A gives a brief description of a similar Finite Difference program used in this thesis. The finite difference method provides a simple algorithm and is well suited to the incorporation of complex constitutive relations.

The finite element method incorporates singularity elements. Due to the similarity of stress fields between the static and stationary crack under dynamic conditions, a static singularity element can be used for the dynamic case. A review of numerical methods in static fracture mechanics is given in[39]. The crack tip element used in PAFEC 75[40] is formed by taking a typical isoparametric element and displacing a mid side node to its quarter position : this gives rise to a $\frac{1}{\sqrt{r}}$ singularity in the strain in the element. The numerical simulation of travelling cracks is reviewed in[20], the main problem being associated with the advancement of the crack. Atluri et al[49] propose a method in which the singularity element moves with the crack tip and the surrounding mesh deforms and adapts to the moving crack.

2.4.3.3. Boundary Element Method

The Boundary Element Method^[41] derives the displacements inside a body due to surface tractions and displacements. The elemental problem of the displacement field due to a discrete traction or displacement is solved using the energetic approach viz the Reciprocal Theorem and Theorems of Maxwell-Betti. This can easily be done for an infinite body, viz the Kelvin solutions, and the task then is to relate this infinite system to the finite system under scrutiny, via Somigliana's identity. The result is that in the solution of a 2D problem only the boundary needs to be specified reducing the problem by one dimension.

The method has been used for static fracture mechanics^[41] but difficulties have been encountered with its use for dynamic crack problems^[42]. Currently harmonically excited problems can be analyzed but the transient problem is intractable. The incorporation of plasticity also presents computing problems.

2.5 Dynamic Initiation Fracture Toughness(K_{Id}) Testing

2.5.1 Introduction

The analysis techniques described so far have been concerned with the growth of stress intensity factor in 2D geometries subjected to dynamic loading. The criteria for crack initiation have been discussed in paragraph 2.3.2.2 and it has been concluded that the two dimensional concept, K_{Id} , may be a valid criterion, although further experimental work is required to validate this hypothesis. *when materials of* engineering interest, e.g. ductile metals, are tested complex three dimensional and plasticity effects occur. In these cases the K_{Id} concept is inapplicable and other fracture toughness parameters have to be sought (see paragraph 2.6). Discussion in this section will concentrate on the brittle failure of materials.

The aim of fracture toughness testing is to study materials of engineering interest in the form of idealized geometries with a view to extracting values of K_{I_d} and showing that such a parameter is a material property. Only then can K_{I_d} values be applied to design problems. As has already been shown the analytic, experimental and numerical techniques are all complex and hence fracture toughness measurements for dynamic loading are scarce. Only one attempt to date^[43] has been made to correlate K_{I_d} values for different geometries. Table 2.2 gives a comparison of K_{I_d} values for 4340 steel using various methods. Note that material properties and initial constraint are often not given for results quoted in the literature.

2.5.2 Stress Wave, Inertial or Quasistatic Loading?

In the discussion so far it has been implicitly assumed that crack initiation is dominated by stress wave loading. In fact fracture can take place in one of 3 regimes - described in table 2.3. As the imposed strain rate increases from the quasistatic, e.g. $\dot{K}_I > 2.75 \text{ MPa} \sqrt{\text{m}} \text{ s}^{-1}$ as defined in ASTM E 399 standard for the 3 point bend specimen, so any dynamic effects start to influence results. In this case a quasistatic analysis can be used but a certain level of error has to be accepted. In the intermediate regime, in which inertia effects are important, either a high level of error can be accepted using a quasistatic analysis or a 'strength of materials' analysis can be contemplated or a full dynamic *calibration* derived. The strength of materials analysis has to be specific to the geometry under study, e.g. DCB^[18] or Charpy^[28]. As a result the derived results are dependent on a mechanical model that includes many assumptions and approximations. The advantage of a full dynamic *calibration* is that it takes into account the full dynamic behaviour of the specimen and hence derived

fracture toughness values can be viewed with high confidence. The disadvantage is that a dynamic calibration is often difficult to derive requiring sophisticated numerical and/or experimental techniques. The above problems can be minimized by using geometries that display a relatively straightforward response or that display a particular, i.e. required, behaviour.

2.5.3 The Compact Tension Specimen (CTS)

The CT Specimen is used extensively in static fracture mechanics and hence is a natural candidate for dynamic work. Two versions have been tested, both in a Split Hopkinson Bar arrangement, namely the wedge loaded CTS[44] and pin loaded CTS[45, 46]. The extraction of fracture toughness values from such tests is problematic. Klepaczko measures the load at fracture from the drop in load on the output bar and uses a static calibration to derive K_{Id} . The test relies on a quasistatic condition in the specimen and knowledge of the frictional condition at the wedge. Corran et al have shown that these two latter conditions are often not met. They used pins to transmit the load into the specimen and hence to give more definable friction conditions. From their tests they conclude that close monitoring of the specimen is necessary and that fracture toughness data cannot be derived purely from measurements obtained from the loading bars. The complex nature of the loading mechanism and geometry means that any dynamic analysis would be complex.

2.5.4 The Instrumented Charpy Test

The Charpy test has been used for many years as a qualitative measure of fracture toughness and hence is a natural candidate for K_{Id} measurements. The test is described in detail in the next chapter, suffice to say that if a dynamic analysis is required it is simpler than the CTS geometry but it still presents difficulties viz bouncing at the supports, nature of input load etc.

2.5.5. Circularly Grooved Bar[43, 47]

A tensile pulse, produced by an explosion, travels down a long (40") circular bar. It is incident on a circular groove with a fatigue precrack. The rising pulse of duration $35\mu s$ fractures the bar in $25\mu s$, typically. The Crack Opening Displacement is measured by an optical extensometer. The plane strain fracture toughness is calculated according to:-

$$K_I = \frac{P}{\pi R^2} \sqrt{\pi R} F\left(\frac{2R}{D}\right)$$

i.e. a quasistatic calibration function. R is the ligament radius, D the overall bar diameter and P the applied load. Due to the small specimen width a quasistatic *calibration* can be used, thus an accurate measurement of K_{Id} can be derived. The test has two disadvantages namely only a high strain rate can be measured and the specimen is very expensive viz a 40" bar with a groove and fatigue crack. Thus the test is relevant for fundamental research but not for industrially orientated materials testing.

2.6 Inelastic Effects

The results of dynamic initiation fracture toughness tests often display extensive plasticity, even at high strain rates. In many engineering situations rupture is dominated by material flow as opposed to cleavage. One major advantage with the elastodynamic approach is its unified and well defined status, hence it has been concentrated upon in this survey.

The inelastic behaviour associated with the Charpy test is discussed in detail in paragraph 3.7. Fracture Toughness can be expressed as the work done by the applied force up to the point of crack initiation. The material failure is a complex mixture of

cleavage and ductile tearing. Plasticity dominated rupture can be analysed using specific material failure criteria, e.g. a plastic strain mean stress parameter (see paragraph 3.7). In the case of elasto-plastic failure the static fracture mechanics concepts of J integral, COD and R curves have been adapted but these concepts are derived and are of equivocal value[51]. Such parameters are geometry dependent and hence are not fundamental material properties. This being so, many technologically orientated researchers have incorporated the J integral into their analyses (see paragraph 3.7).

Nilsson[9] reviews the current analytic models for crack tip fields for dynamically loaded bodies. He considers the cases of elastic perfectly plastic material, linearly strain-hardening elastic plastic material, power law hardening material and elastic viscoplastic material. These analytic models are used in the generation of fracture parameters and the specification of parameters to be sought for material failure criteria.

The experimental method of caustics can deal with limited plasticity[30] but most experimental methods are firmly based on linear elastic behaviour. Numerically various constitutive relations can be modelled (see Appendix A) but the specification of material damage is still problematic.

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Table 2.1 Wave Velocities for Given Waves and Materials

	Steel	Araldite	Polyurethane
E STATIC [Pa]	2.1×10^{11}	3.25×10^9	10^8
ν STATIC	0.29	0.36	0.48
ρ [kgm ⁻³]	7860	1120	1100
λ [Pa]	1.48×10^{11}	3.07×10^9	1.12×10^8
μ [Pa]	0.81×10^{11}	1.19×10^9	0.34×10^8
$C_0 = \sqrt{\frac{E}{\rho}}$ [ms ⁻¹]	5169	1703	302
$C_1 = \left[\frac{\lambda + 2\mu}{\rho} \right]^{\frac{1}{2}}$ [ms ⁻¹]	6280	2206	406
$C_2 = \left[\frac{\mu}{\rho} \right]^{\frac{1}{2}}$ [ms ⁻¹]	3210	1031	176
$C_R = C_2 \left[\frac{0.87 + 1.12\nu}{1 + \nu} \right]$ [ms ⁻¹]	2973	965	167
C_g^1 [ms ⁻¹]	3101*	-	80 ⁺

$\lambda = \frac{\nu E}{(1+\nu)(1-2\nu)} \qquad \mu = \frac{E}{2(1+\nu)} \qquad E = \frac{\mu(3\lambda + 2\mu)}{\lambda + \mu}$

* $c_g^1 \simeq 0.6c_0$ for steel [2] + Taken from high speed photographs

- C_0 = velocity of longitudinal waves of infinite wavelength in a bar
- C_1 = velocity of dilatational waves in an unbounded medium
- C_2 = velocity of distortional waves in an unbounded medium
- C_g = group velocity of longitudinal waves in a bar
- C_p = phase velocity of longitudinal waves in a bar
- C_R = velocity of Rayleigh surface waves
- C' = phase velocity of flexural waves in a bar
- C_g' = group velocity of flexural waves in a bar

Table 2.2 Comparison of K_{Id} values derived for 4340 steel

Geometry	Researchers	Temp- era- ture	Const- raint	K Id MPa $\sqrt{m}$	R C	$\dot{K}$ I MPa $\sqrt{m} s^{-1}$
Charpy	Costin, Duffy et al [47]	RT	FC	58	48	2.2×10^5
Circularly Grooved Bar	Costin, Duffy et al [47]	RT	FC	56	48	2.2×10^6
Charpy	Kanninen et al [48]	RT	N	170	-	$\sim 10^6$
Charpy	Kanninen et al [48]	RT	N	65	-	$\sim 10^6$
Charpy	Nishioka et al [49]	RT	NS	108	-	$\sim 10^6$
CTS	Klepaczko [44]	RT	FC	50 ⁺	-	$0.02 < \dot{K}_I < 10^6$
SEN	Homma et al [50]	RT	FC	32	51	8×10^6

+ '0.45% Carbon Steel' RT - Room Temperature NS - Numerical Singularity
FC - Fatigue Crack
N - Notch

Table 2.3 Loading Regimes for Dynamic Initiation Fracture
 Toughness Testing

$0 \leq \dot{K}_I \leq 10^3$	Quasi-static	Conventional Testing M/C
$10^3 \leq \dot{K}_I \leq 10^5$	Medium Rate	Hydraulic M/C. Notched Bar Impact Tests
$10^5 \leq \dot{K}_I \leq 10^9$	High Rate	Stress Wave Loading

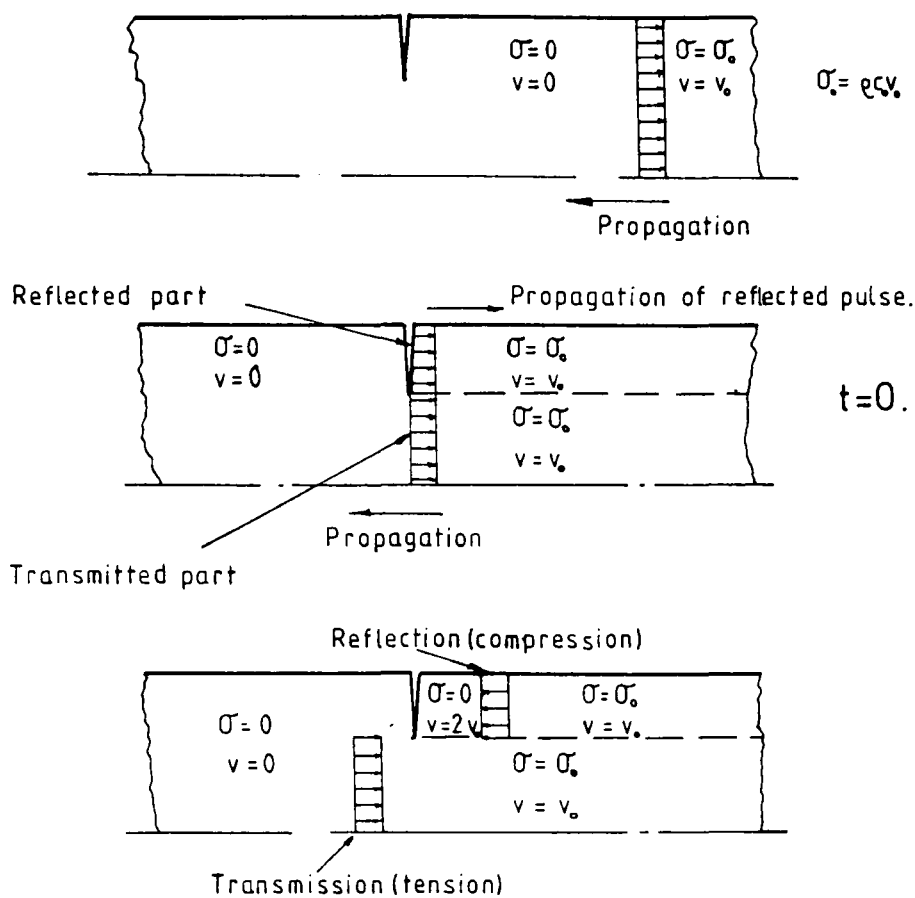


Fig. 2.1 Discontinuity in particle velocity in a Circularly Grooved Bar subjected to a tensile pulse

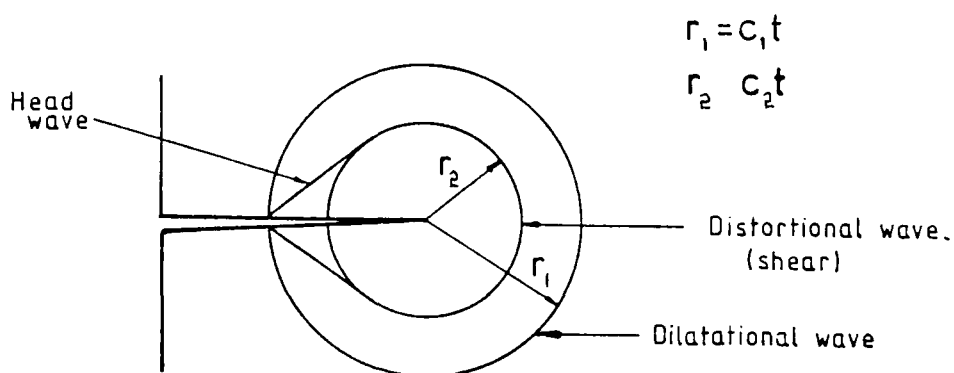


Fig. 2.2 The diffraction of stress waves about a stationary crack

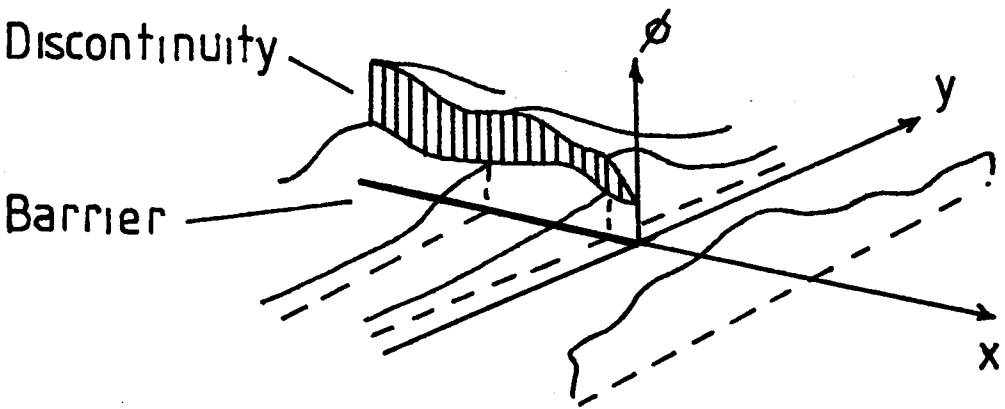


Fig. 2.3 Wave potential, ϕ , about a semi infinite crack

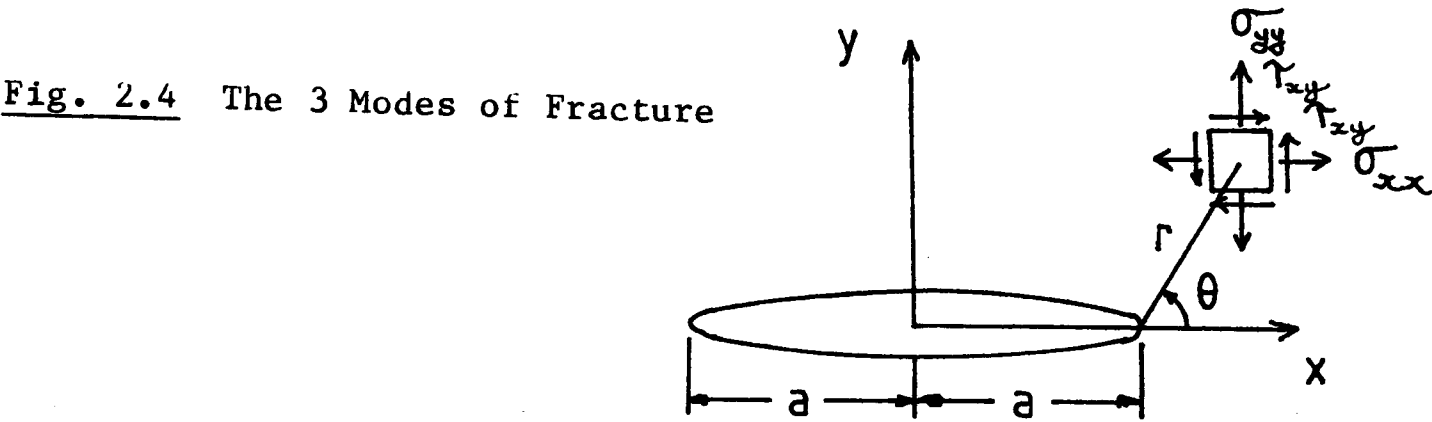
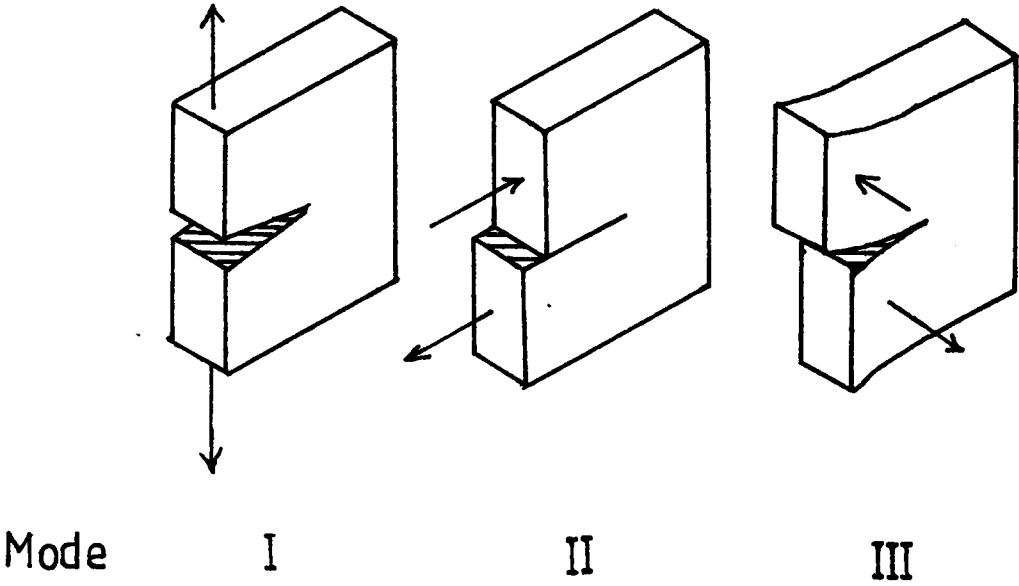


Fig. 2.4 The 3 Modes of Fracture

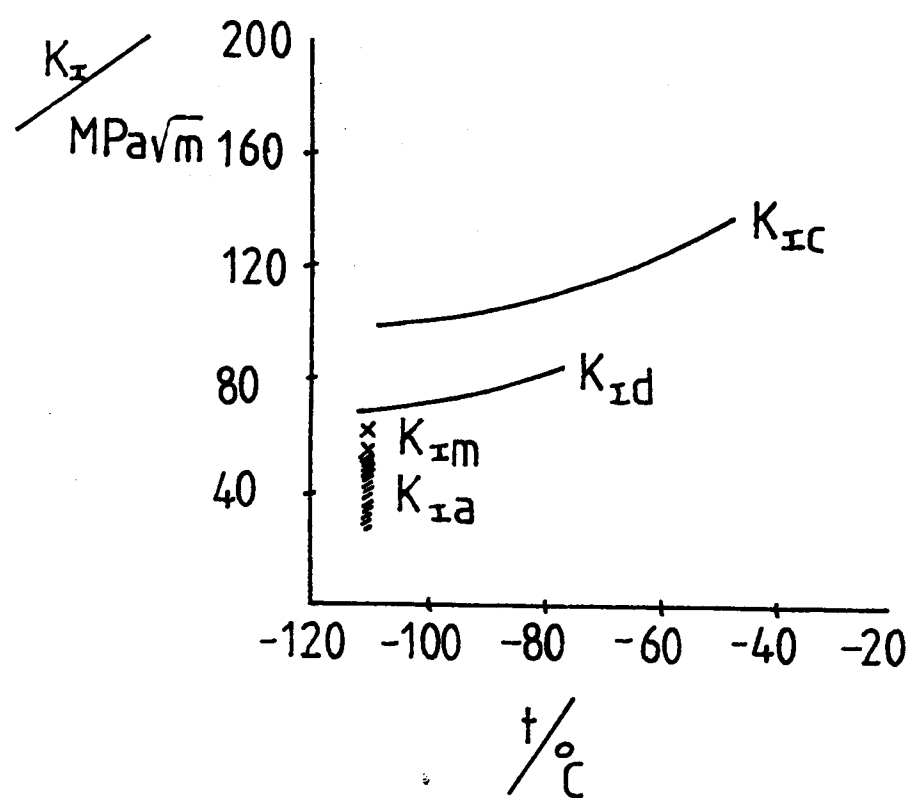


Fig. 2.5 K_{IC} , K_{Id} , K_{Ia} and K_{Im} values for Mn - Ni Steel

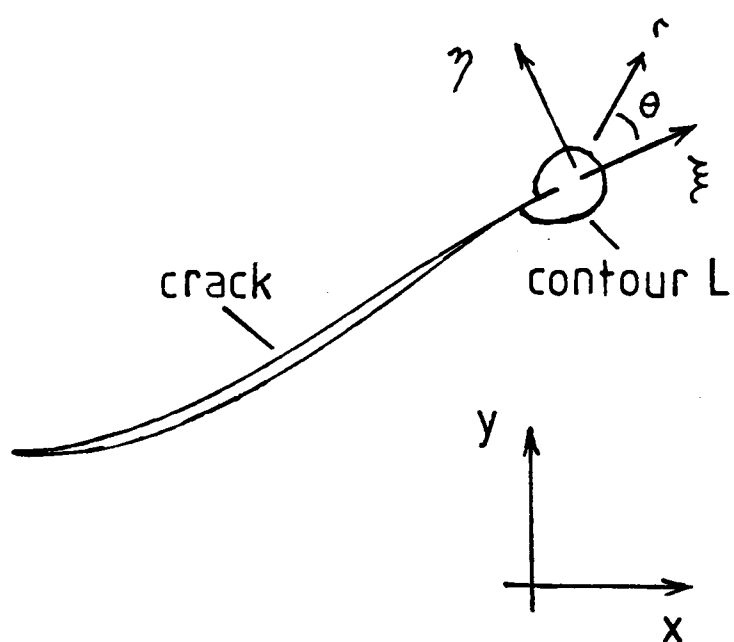


Fig. 2.6 Coordinate system for a propagating crack

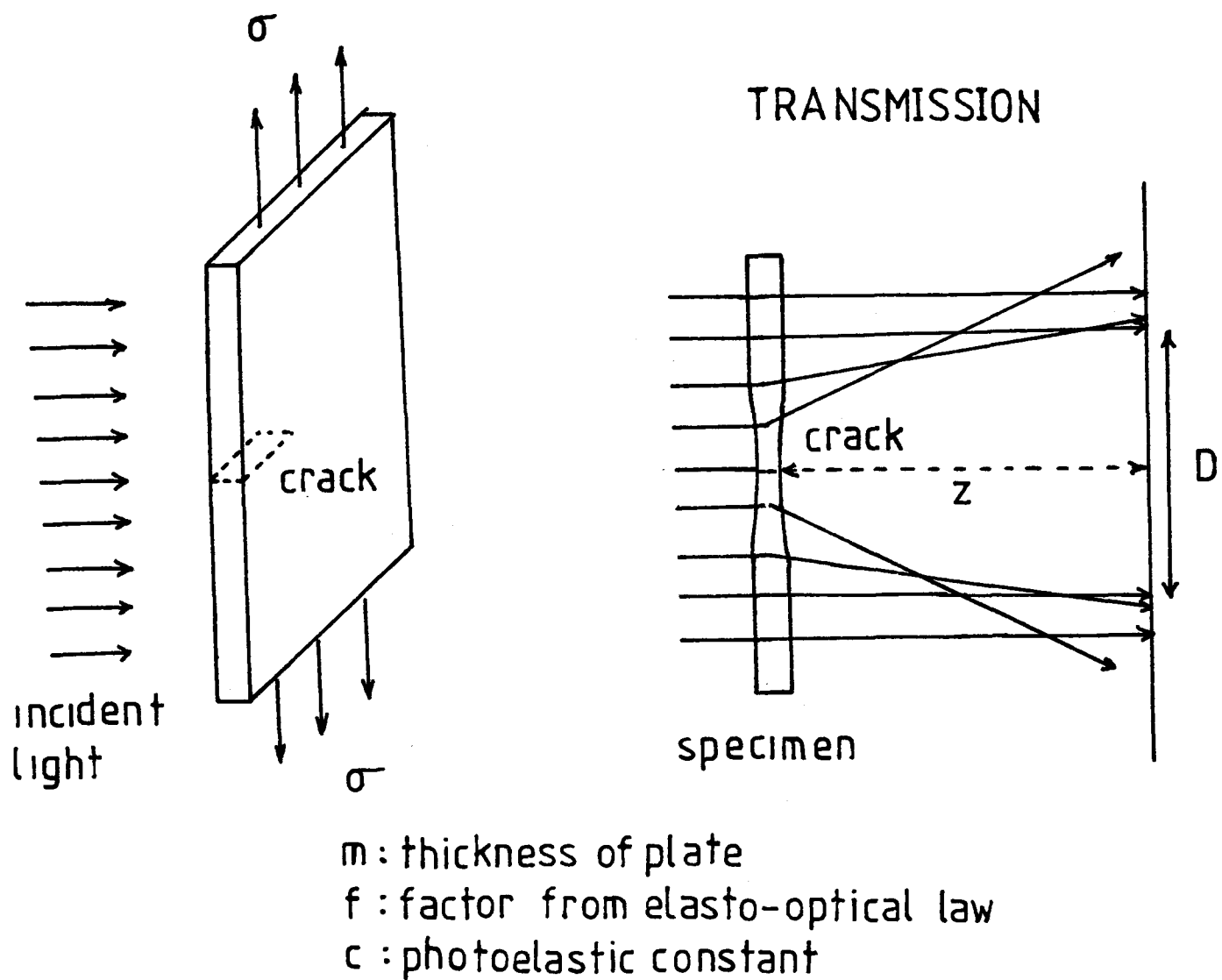


Fig. 2.7 Formation of and Measurement from a Caustic

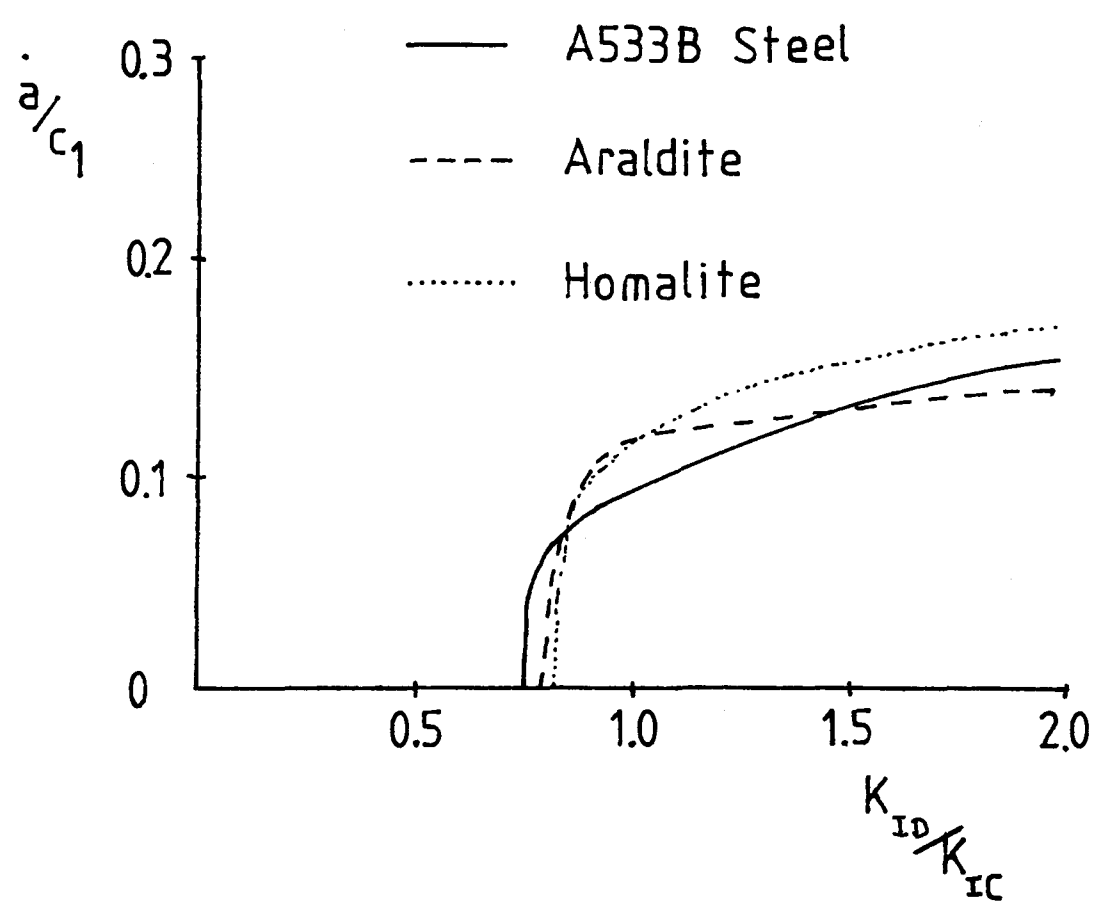


Fig. 2.8 Crack Resistance or K_{ID} curves

CHAPTER III

THE INSTRUMENTED CHARPY TEST

3.1 INTRODUCTION

The simple Charpy Test has been used since 1900 to test the resistance of a material to brittle crack growth. Brittle fracture is promoted by a notch, i.e. stress triaxiality, by high strain rate, i.e. impact, and by the lowering of the temperature of the material. Thus although these three environmental conditions do not agree directly with service conditions, it is felt that an idealized test incorporating these factors is valid in a qualitative but conservative sense. In the past the test has mainly been used as a qualitative measure of a material property and the value of the test lies in the comparison of like materials. As fracture mechanics has developed so there have been attempts to quantify the test; in other words to relate the notch toughness to standard fracture parameters, e.g. K_{Ic} . Initially, these correlations were purely empirical but more recently a more sophisticated approach has been tried, viz. the Instrumented Charpy Test.

3.2 THE DEVELOPMENT OF THE INSTRUMENTED CHARPY TEST

Traditionally the notch toughness characteristics of low and intermediate strength steels have been described in terms of the transition from brittle to ductile behaviour, as measured by energy absorbed, lateral expansion or percentage of fibrous fracture. The question to be asked is 'what level of performance should be required for satisfactory performance in a particular structure?',^[1]. The transition curve approach gives only approximate, qualitative, design values.

In an effort to gain more accurate information from notch toughness tests, which are still used extensively for material development and quality control purposes, various correlations have been attempted using the extensive data available viz. (from [1]):

1. K_{Ic} - CVN Upper Shelf Correlation.
2. K_{Ic} - CVN Correlation in the Transition Temperature Region.
3. Approximation of Entire K_{Ic} Curve from CVN Impact Data.

Such correlations are only valid for a restricted type of material, e.g. steels having $\sigma_Y > 630$ MPa, and depends on having a large number of results to analyse. Thus although the approach may be technologically valid, it is very restrictive and of equivocal value for the generation of more fundamental data.

To gain more information, the impacting weight can be strain gauged (see Fig. 3.1). The signals from the strain gauges are amplified, possibly filtered, and stored electronically. The resultant trace is taken to be the force applied to the specimen - for typical examples of elastic and elasto-plastic behaviour see Fig. 3.2. From these traces it is hoped to measure the time of crack initiation and the force at which initiation occurs. This is reasonably straightforward for elastic behaviour but is more problematic for inelastic tests. The angular displacement of the pendulum can be recorded and related to the displacement of the leading edge of the impacting weight.

In fact, Instrumented Charpy Machines can be of the 'C' weight pendulum type (as shown in Fig. 3.1), or be of the 'U' weight type or be in the form of a drop weight. Standard systems on the market are:

1. Dynatup (Effects Inc., U.S.A.).
2. Avery-Dennison Pendulum with Ceast Instrumentation^[2].
3. Amsler Wolpert (^C/o Dartec Ltd., U.K.).

The specification of the Charpy test is defined in ASTM standard E23,

but this, and other standards, leaves a lot of room for interpretation by machine builders and testers. If more accurate data is to be obtained from the Charpy test, then the test needs to be defined more closely.

3.3 STANDARDIZATION AND CLOSER SPECIFICATION OF THE INSTRUMENTED CHARPY TEST

Given the technological requirement, mainly from the nuclear engineering industries, to derive fracture toughness values from Instrumented Charpy Tests some effort has been made in standardizing and specifying the test more closely^[4]. Four authorities have looked at the test viz.:

1. EPRI - Electric Power Research Institute.
2. ASTM - American Society of Testing of Materials.
3. ASK - Swiss Nuclear Safety Division.
4. BS - British Standards.

The most widely discussed standard is the EPRI one, which has been adapted by ASTM,^[3] and so this will be discussed in detail. The general test requirements can be summarized as:

(i) $t_f \geq 3\tau$ where ' τ ' is the period of apparent oscillation of the specimen.

(ii) $t_f \geq 1.1 T_R$ where ' T_R ' is a measure of the response time of the instrumentation.

(iii) $E_o \geq 3W_M$ where W_M is the system energy dissipated to maximum load. (This requirement is to ensure that the reduction in pendulum velocity during fracture is less than 20% so that the test can be thought of as dominated by a constant rate of deformation.) If these requirements are satisfied and an elastic response occurs (see Fig. 3.2) then the static *calibration* (4) at maximum load, P_M , (5) gives the initiation fracture toughness, K_{Id} . If an elasto-plastic response occurs, energy values can be derived to obtain a J integral

value^[3], but such a parameter suffers from the same difficulties as in static fracture mechanics^[5]. Server, in [4], has used data derived in this manner, from elastic tests, with CTS tests under inertial loading, to obtain a plot of K_{Id} v. $\log \dot{K}_I$ (see Fig. 3.3). Thus from this data the static fracture toughness, K_{Ic} , can be deduced. The embrittling effect of strain rate means that smaller specimens give rise to plane strain dominated fracture than would otherwise be the case under quasistatic loading. Although the above methodology is widely used, values obtained are often questioned^[4] and a more extensive discussion of the test is required.

3.4 DISCUSSION OF THE GENERAL TEST REQUIREMENTS AND PROCEDURES

3.4.1 Dynamic Behaviour Prior to Crack Initiation under Elastic Conditions

As can be seen from Fig. 3.2, inertial oscillations are superimposed on a rising mean load. The '3 τ ' requirement requires that the specimen should fracture after three oscillations. This criterion has no scientific basis but is based on the dissipation of oscillations in an unsupported beam^[11]. The oscillations decay due to material damping and inelastic processes. In the case of purely brittle fracture, this criterion is often violated (see Table 3.1) and in the case of elasto-plastic behaviour the point of crack initiation is often difficult to discern (see Section 3.4.2).

The dynamic behaviour of the Charpy Test has been a subject of study for many years - one of the earliest extensive discussions being in 1920^[6]. The first dynamic analysis of the impact test was due to Lamb, in [6], treating the specimen on its supports as a one degree of freedom system with damping. The equation of motion can only be regarded as a relationship between values that have to be measured experimentally. A more sophisticated mechanical model has been described by Glover et al.^[7] and Turner^[12]. The simple linear spring

mass model is shown in Fig. 3.4. The beam test piece is represented as an effective point mass, m_e , carried on a spring of equivalent stiffness k_e . This equivalent stiffness is itself a combination of the stiffness of the notched beam, k_n , in series with the stiffness k_r of the reacting supports. The pendulum moves at a constant velocity V and transmits load via a spring of stiffness k_p , equivalent to the response of the striker and indentation of the test piece. The solution of the differential equation of motion^[7] gives the striker force, F :

$$F = \frac{k_p V}{p(1 + \frac{k_p}{k_e})} (pt + \frac{k_p}{k_e} \sin pt)$$

where

$$p = \sqrt{\frac{k_n + k_p}{m_e}}$$

This is shown in Fig. 3.4 and is a sine wave superimposed on a steadily rising ramp, similar to the early elastic response of Fig. 3.2. The period of this oscillation is:

$$\tau = 2\pi \left[\frac{CSBW\rho}{k_n(1 + \frac{k_p}{k_e})} \right]^{\frac{1}{2}}$$

where $m_e = Cm = \frac{17}{35} m$ from conventional beam vibration theory. k_p , the stiffness of the pendulum-specimen interaction, depends on the configuration of the nose of the striker, the modulus of the beam and its yield point, if there is any plasticity. This value of contact stiffness can be estimated from the initial slope of F (see Fig. 3.4):

$$k_p = \frac{p \tan \alpha}{V}$$

and it can be seen that it may very well be variable for different pendulum/drop weights and specimen materials. k_e can be estimated from subtracting k_p from the total stiffness k_s where:

$$k_s = \frac{p \tan \beta}{V} \text{ and } \frac{1}{k_s} = \frac{1}{k_p} + \frac{1}{k_e}.$$

k_e should be reasonably constant for ASTM E23 tests. Hence k_p , k_e and k_r , which equals $\left[\frac{1}{k_e} - \frac{1}{k_n} \right]^{-1}$ where k_n is the stiffness of the notched beam, can be determined from two experimental traces for differing notch depths. Table 3.2 compares the τ values, derived from first principles, with those obtained from empirical study. Note that the last two τ identities are equivalent for $\frac{S}{W} = 4$, which occurs for ASTM E23 standard tests. For equivalence between the two approaches $k_p \approx 6k_e$, i.e. the beam should be less stiff than the pendulum and supports.

This simple mechanical model suggests that the response of the specimen, described by τ , for example, is dependent on its testing environment. This fact is borne out by the diversity of load traces that exist for the Instrumented Charpy Test in the literature. Turner's model is over simplistic in that it ignores the inertia of the supports^[8,12] and neglects any damping^[12].

Mines and Ruiz^[8] have modelled the test as a continuous system with simple supports under central impact in the form of a ramp load. From the analysis, the bending moment at the centre is:

$$BM = \frac{P(t)S}{4} \left[1 - \frac{8}{\pi^4 t} \frac{mS^3}{EI} (\sin w_1 t + \frac{1}{27} \sin w_3 t + \frac{1}{125} \sin w_5 t + \dots) \right]$$

where $\frac{P(t)S}{4}$ is the static bending moment. If $t \gg \frac{1}{w_1}$, then $\frac{8}{\pi^4 t} \frac{mS^3}{EI}$ is very small and the bending moment is quasistatic. In other words $t \gg \frac{\tau}{2\pi}$ where $\tau = \frac{2}{\pi} \frac{mS^3}{EI}$, the first order frequency. For an unnotched beam of length 40 mm, i.e. a Charpy Specimen with no overhang, $\tau = 68 \mu s$ and $t_f \gg 11 \mu s$. This differs from that value from the empirical formula (see Table 3.1), i.e. $\tau = 26 \mu s$ and $t_f > 72 \mu s$. The difference is due in part to the effect of the testing environment on the response of the specimen, e.g. the stiffness of the impactor and supports. The response of the specimen is also dependent on the applied force, i.e. a step input instead of a ramp loading in the

latter model gives a completely different response^[8]. The complex relationship between the applied force and resulting bending moment in a real Charpy test has been studied by Nash^[10]. He has shown that although the applied force oscillates in a typical Instrumented Charpy Test the central bending moment increases monotonically (Fig. 3.5). Turner^[12] ascribes this to natural frequency mismatch between the whole system and the specimen acting on its own.

Kalthoff^[15] and Kobayashi^[9] have shown that a similar response occurs in the growth of stress intensity factor, K_I (see Fig. 3.6). Of course the bending moment and stress intensity factor have to be calculated as dynamic quantities, and so if a quasistatic calibration is used the large oscillation in the load will be reflected in the moment or stress intensity. From his simple two degree of freedom model Turner^[12] has shown that the '3 τ ' criterion can be compared to a reduction in overall test piece energy, to $\pm 10\%$ of the mean, provided that the specimen is not too compliant, i.e. $\frac{k_p}{k_e} < 6$.

Thus a static calibration at maximum load is only as accurate as the departure of tup load from the mean ramp load (see Fig. 3.2a). This departure is dependent on the speed of impact and the response of the system. An empirical correlation may be possible between the mean ramp load and the dynamic growth in bending moment and stress intensity. As the speed of impact decreases so this relationship will become quasistatic in nature. Further discussion of simple mechanical models is given in [8, 12] which includes the variation of apparent system stiffness with impact velocity and the effect of scaling inertia response.

3.4.2 Crack Initiation for Elastic and Elastoplastic Conditions

Table 3.1 gives t_f values for various 3 point bend test results embracing a wide variety of materials and geometries. It can be seen that in nearly every case the '3 τ ' criterion is violated, but of

course the ' 3τ ' criterion derived by Server is based on a large number of tests on Charpy size specimens of low and intermediate strength steels. Note also that $P(t)$ characteristics for the different tests given in Table 3.1 are often completely different from the standard Charpy response.

The main aim of the Instrumented Charpy Test is to obtain all the required information from the $P(t)$ curve. This includes the time of crack initiation and the load at initiation. Server^[3] proposed that in the elastic case the crack initiates at maximum load. Kalthoff et al.^[14] have shown that for a $0.55 \times 0.1 \times 0.01$ m Araldite specimen, with a notch, struck at 5 ms^{-1} , this is not the case. In the case of the metal Charpy specimen limited plasticity may delay the onset of crack initiation. It is suggested that an independent measure of crack initiation, e.g. a gauge adjacent to the crack, is required for very brittle fracture with negligible shear lips. If $t_f > 3\tau$ then a quasistatic *calibration* may be used, given the discussion in the previous paragraph.

Up until now we have been discussing crack initiation under elastic behaviour. Many Charpy tests display some plasticity. In this case the response is shown rather idealistically in Fig. 3.2 - a more realistic response shows a gradual change from elastic to plastic behaviour. (See, for example, Figs. 6.13 or 6.14.) Note that a change in gradient in the $P(t)$ curve can either be due to plasticity or due to a change in *calibration* as a result of crack initiation. T. Kobayashi^[21,22] filters out the inertial oscillations leaving a steadily rising curve that changes gradient when plasticity occurs. If crack initiation can be measured then a J value can be derived. The J value is taken as the measure of the energy input into the specimen up to crack initiation. It has been shown that the $P(t)$ trace is often difficult to interpret for the elasto-plastic case thus

there is scope to develop an independent measure of initiation time viz. potential drop, use of irradiation, use of magnetic flux or the use of a 'low blow' procedure. One way the situation can be improved is by increasing the crack tip constraint, i.e. by fatigue pre-cracking the specimen, and hence promoting brittle fracture.

Thus even if $P(t)$ can be measured, the time of crack initiation is often difficult to discern. The interpretation of empirical data is complicated by the diversity of materials and geometries used, not to mention the methods of impact.

3.4.3 Conclusions on the General Test Requirements and Procedures

It has been shown that for a restrictive class of materials and geometry, e.g. ASTM E23 Charpy specimen, the proposed criteria give values that are not always correct for an elastic response. But as soon as the testing conditions are changed then the criteria become invalid. It has also been shown that for an elastic response, crack initiation does not necessarily occur at maximum load and that for many Charpy tests some plasticity occurs which makes crack initiation conditions often difficult to interpret. The specimen can be impacted more slowly^[29] or soft pads put between the impactor and specimen^[19] but this is diverging from the Charpy standard and reduces the embrittling effect of strain rate. A list of various loading rates and interested authorities is given in Table 3.3. Larger specimens can be used, enhancing plane strain conditions but compromising the advantage in the Charpy test of using small and cheap specimens. If more accurate data is to be obtained and if the test is to be widened to other materials and, possibly, other geometries then a more fundamental analysis of the test is required - this includes the derivation of a dynamic calibration.

3.5 MEASUREMENT INSTRUMENTATION

The accurate analysis of $P(t)$ characteristics requires the accurate storage of data. The dynamic response of bridge circuits, differential amplifiers and storage systems should not alter the raw data. This condition is given by the criterion $t_f \geq T_R$. Fig. 3.12 gives an example of a load trace from a pendulum Instrumented Charpy Test. The specimen yields at point A and hence the derivation of a J value needs to be considered for this test. Are the oscillations in the $P(t)$ trace the applied load or a result of the dynamic response of the pendulum and instrumentation? Note that once the specimen has broken there is 'ringing' in the pendulum of period 75 μs . This is close to the 35 μs period of the 'inertial' oscillations. τ , derived from McConnell and Server's empirical formula (see Table 3.2) for an $\frac{a}{W} = 0.5$, is 45 μs . Thus the instrumentation may well be influencing the load signal in this case. The performance of the entire measurement system needs to be fully defined, but even then the strain gauge on the impacting weight will only give a quasistatic load and only inertial or stress wave effects in the weight will influence results.

3.6 THE ELASTO-DYNAMIC CALIBRATION OF NOTCHED BEND SPECIMENS

3.6.1 Introduction

The derivation of a dynamic *calibration* is a difficult procedure. It was shown in Chapter II that no closed form analytic solution exists for an elasto-dynamic response, thus experimental and numerical methods need to be used. The most unequivocal, experimental method is due to Kalthoff et al.^[14,15] described in Section 2.4.2.1, but other methods have also been proposed.

3.6.2 The Caustic Method of Kalthoff et al. [15]

The methodology is as follows:

1. Obtain $K_I(t)$ for a specimen tested in the apparatus to be used. The specimen is made from a notch tough material of the same stress wave characteristics as the material to be tested and the specimen has a notch instead of a fatigue pre-crack. In this way fracture does not occur.
 2. Calibration curves, i.e. $K_I(t)$, are obtained for various striking velocities.
 3. Fatigue pre-cracked specimens are tested under exactly the same conditions and the time to fracture measured.
 4. K_{Id} is derived from $K(t_f)$, i.e. from calibration curves.
- Such a method is direct but requires sophisticated experimental apparatus. The specimen might have to be oversized in order to contain the caustic. The fact that the response of the specimen also depends on the nature of the impactor and supports means that calibration curves are required for each different apparatus.

3.6.3 Loss' Procedure [9,16,17]

It has been shown that although the applied force may oscillate, the bending moment and stress intensity factor do not. Thus a strain gauge in the vicinity of the notch should give a non-oscillatory behaviour. The procedure is:

1. In a static test, measure load P_{stat} and the stress σ_{stat} at the strain gauge position.
2. Using a static calibration between P_{stat} and K_I , obtain an overall static calibration function:

$$K_I = f(\sigma_{stat}).$$

3. In an impact test, measure σ_{dyn} with the same strain gauge

and calculate $K_I(t)$ from:

$$K_I(t) = \Gamma(\sigma_{\text{dyn}})$$

Although this methodology assumes a static relation, Γ , the method has been extensively validated using a finite element analysis, HONDO^[17]. The validation has been carried out for oversize specimens, viz. 177.8 mm x 41.3 mm x 15.9 mm, made out of steel and aluminium. Also the behaviour has been predominantly elastic. There is some disparity between the dynamic strains measured numerically and experimentally - especially for the aluminium specimen. Typical times to failure have been 400 μs . As a test becomes more dynamic so the analysis may well become inaccurate, viz. a PCVN specimen which fails in 150 μs . In the finite element analysis the stress intensity factor is computed from a Crack Opening Displacement. Loss' analysis compares with Kalthoff's (see Fig. 3.6), i.e. although the applied force oscillates, $K_I(t)$ increases monotonically.

3.6.4 The 'Crack Arrest' Approach of Kanninen et al.^[18,23,24]

Given the extensive amount of data on crack propagation and arrest tests, it would be convenient to use the data for crack initiation problems. The K_I v $\dot{a}$ curve, for 4340 steel (see Section 2.3.2.4) can be characterised as:

$$K_{ID} = 65 + 0.044\dot{a}$$

for steel. This was checked by a finite difference program but on analysing impact tests the identity was found to be grossly inaccurate, a better value being $K_{ID} = 170$, i.e. independent of crack velocity. The above disagreement is not surprising given the difference in mechanics of crack arrest and crack initiation^[25].

Kanninen et al.^[23] think that the disparity is due to plasticity effects. Nishioka et al.^[24] have looked at the problem in an

exhaustive manner using a numerical analysis incorporating a moving singularity element. They conclude that a K_{ID} of $108 \text{ MPa}\sqrt{\text{m}}$ is more appropriate but the scatter in their results is of the order of 50%. Note that Costin et al.^[20] derive a K_{ID} of $58 \text{ MPa}\sqrt{\text{m}}$ for the Charpy test.

3.6.5 Conclusions on the Derivation of an Elasto-Dynamic Calibration

It can be seen that the generation of a dynamic calibration can become complex if indirect methods are used, and in these cases physical insight into what is going on can be lost. The aim is to derive accurate values of K_{ID} . Server's quasi-static approach may yield a reasonably accurate value of fracture toughness for a specific set of conditions which include a single, or limited, loading rate. But to widen the loading rate, a dynamic calibration is required. The decision to be made now is should one continue to concentrate on the Charpy geometry or should one go towards a larger specimen tested under more scientific conditions.

The technological pressure is to continue with the Charpy test - but the best one can hope for is a limited number of data points on the K_{ID} v. K_I curve. Thus extrapolation back to K_{IC} would be difficult and K_{ID} at other loading conditions would be impossible. On the other hand a K_{ID} value would be obtained at varying temperatures, giving further data over and above the CVNE data.

For a more scientific and systematic evaluation of K_{ID} values the Charpy test has to be adapted and more sophisticated analysis methods used. In this way K_{ID} values would be obtained for other geometries. If this is the case, the caustics method of Kalthoff et al. is the most direct and accurate.

A further possibility, from the technological point of view, is to keep the 10 mm x 10 mm x 55 mm envelope of the Charpy geometry but to look for a simpler geometry, that would yield K_{ID} values for a

wider range of K_I values.

3.7 INELASTIC EFFECTS

In the dynamic analyses described above it has been assumed that elastic behaviour predominates. In many tests on engineering materials extensive plasticity occurs. This situation can be improved by increasing the notch tip constraint, i.e. by fatigue pre-cracking the specimen to a depth of $\frac{a}{W} = 0.5$, typically. Further improvement can be sought by impacting the specimen faster but an upper limit occurs above which a dynamic compliance is required. Most proposed methodologies for Instrumented Charpy Testing include the derivation of elasto-plastic parameters, e.g. [3, 4, 21, 22, 26, 27]. For example Koppenaal^[27] uses an equivalent energy method to extract equivalent K_{Id} values from plastic response load curves. T. Kobayashi [21,22] measures crack initiation using a plastic range low blow test, in which initiated specimens are subsequently colour tinted and opened to correlate specimen deflection and initiation. J_{Id} is then measured from a load-deflection curve up to the point of initiation. Iyer and Miclot^[26] derive energy values up to limit load for specimens of different crack length and derive a J value from the rate of change of energy with crack length. Ritchie^[5] has shown that for static fracture mechanics J values can be geometry dependent and similar problems are encountered in the dynamic case.

An alternative approach to an elasto-plastic analysis is a fully plastic study in which the singularity at the notch tip is no longer dominant. A plastic analysis of a simply supported beam under central impact is given in [8]. The beam is modelled assuming rigid-ideally plastic material and a plastic hinge is developed at the centre whilst peripheral hinges form at A and A' (see Fig. 3.8) and travel until they reach the supports. A failure criterion can be derived as

equality between Charpy V notch energy, CV, and the work done by the plastic hinges. For a notched beam:

$$t_f \cong \frac{S}{4M_{p_o}} \frac{(CV)^2}{T_i}$$

From this analysis it can be seen that the time to failure is dependent, in a linear manner, on the span.

A much more sophisticated, numerical plastic analysis is described in [28] using the HEMP finite difference code. The specimen is modelled with a 'V' notch only. The impactor is numerically modelled. The failure criterion is given by a plastic-strain, mean-stress parameter^[30]. Results show that when crack initiation occurs only 10% of the available energy is used showing that there is no fundamental basis to the K_{Id} v. CVNE correlation. A similar conclusion has been reached for the elastic fracture of Homalite (see Fig. 3.7 taken from [9]). Norris et al. also discuss the adiabatic heating effect at the notch tip, that no doubt influences material properties and failure.

It is suggested that these fully plastic analyses be developed to give an upper bound to the dynamic behaviour of the Charpy Test, and as an aid to the development of material failure criteria under ductile conditions.

3.8 CONCLUSIONS ON INSTRUMENTED CHARPY TESTING

The K_{Ic} - CVNE correlation gives fracture toughness values from impact tests using an empirical and statistical approach based on an extensive number of tests. The resultant values are only approximate and display wide scatter. The Instrumented Charpy Test improves this situation giving more accurate values of fracture toughness, albeit for a restricted range of materials. But the method is still prone to error due to its inherent assumptions and approximations. The

fracture toughness data is obtained for varying temperatures and a K_{Id} v. Temperature plot derived. Each fracture toughness value for each temperature, refers to a single strain rate, $\dot{K}_I$.

If the fracture toughness value is to be more accurate the Instrumented Charpy Test needs to be improved. It is suggested that the Hopkinson Pressure Bar method of loading should be used (see Chapter VI). If fracture toughness values are to be obtained at various $\dot{K}_I$ values and the restrictive conditions of the current Instrumented Charpy Tests are to be avoided then a more scientific approach is required. The 3 point bend specimen needs to be enlarged and a dynamic *calibration* considered. Given the complexity of the latter, alternative geometries also should be sought, which display simpler *calibration* functions. In the effort to see whether K_{Id} is a material property accurate K_{Id} values need to be derived for varying geometries.

Any elasto-plastic behaviour complicates the response and data reduction. Given the equivocal nature of dynamic elasto-plastic fracture parameters, the elastic response is concentrated upon in this thesis. Of course the embrittling effect of strain rate can be used to enhance the elastodynamic behaviour.

3.9 THE TENSILE LOADED NOTCHED BAR CONFIGURATION

It has been shown that the elastodynamic response of the Charpy geometry is complex and that there is a requirement to develop a geometry that has a more simple dynamic behaviour. The Circularly Grooved Bar Dynamic Fracture Toughness Test has already been described (see Section 2.5.5) and shown to give rise to a well behaved response. This geometry will be adapted, later in the thesis, into the form of a Two Dimensional Double Notched Bar. A brief survey will now be given of the problem of a surface piercing notch subjected to tensile

loading. An extensive analysis of a single edge notch specimen has been undertaken in Germany and the U.S.A., and is described in [31] for epoxy specimens and in [32] for metal specimens. The specimens and loading arrangements for the tests are given in Fig. 3.9. In the case of the epoxy specimen the notch is stress conditioned in compression prior to being subjected to tension due to stress unloading from face A. The tensile loading is non-oscillatory and hence the growth of $K(t)$, i.e. the dynamic *calibration*, is also well behaved. Stress intensity factors are derived from the caustics by means of multiple spark photography. The variations of σ and $K_I(t)$ are given in Fig. 3.10. The apparatus, shown in Fig. 3.9b, gives a stress pulse of duration dependent on the projectile length and of amplitude dependent on the speed of impact. The variations of $K_I(t)$ for different pulse durations are given in Fig. 3.11, derived from a finite element analysis. Given the relatively simple behaviour of the specimen, the pulse duration and amplitude can be systematically varied and Homma et al.^[32] have found that there is an incubation time for crack initiation. In other words the stress intensity has to exceed the critical value for a finite time before initiation occurs. This incubation time could be a material property.

Thus a specimen and loading system similar to the above would seem to offer potential for development. In this research a two-dimensional adaption of the circularly grooved bar was selected and the loading apparatus and specimen were developed with a technological approach in mind, i.e. a simple and cheap specimen with modest instrumentation and loading apparatus.

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Table 3.1 Summary of various 3 point bend test results

	a / W	Span (m)	Depth (m)	Thick ness (m)	γ^+ (μ s)	t_f/γ	v_{imp} ($m s^{-1}$)	t °C	Notch or Crack	K_{Id} MPa $\sqrt{m}$
Photo flex Tests (Ch. 4)	0.22	0.092	0.025	0.005	1617	-	3.5	RT	V	-
Araldite Tests (Ch. 5)	0.22	0.092	0.025	0.005	208	1.73	3	RT	N	2.8
ASTM E23	0.20	0.040	0.010	0.010	29	> 3	$3 < v < 6$	-	V	-
ASTM E23 (Unnotched)	0	0.040	0.010	0.010	26	-	-	-	-	-
ASTM E23 (Pre-cracked)	0.50	0.040	0.010	0.010	45	> 3	$1.2 < v < 4^+$	-	FC	-
Homalite ^[9]	0.28	0.368	0.089	0.0095	783	0.24	1.72	RT	FC	0.4
Polycarbonate ^[9]	0.28	0.368	0.089	0.0032	980	1	1.72	RT	FC	2.5
A533B Steel ^[9]	0.53	0.204	0.051	0.025	354	$1.2 - \frac{2}{2}$	2.5	RT	N	123
J22 Steel ^[10]	0.31	0.178	0.041	0.016	161	0.43	9	-	N	205*
6061 Aluminium ^[10]	0.31	0.178	0.041	0.016	166	0.72	9	-	N	47*
6061 Aluminium ^[9]	0.42	0.166	0.041	0.016	173	0.81	8.6	RT	FC	44
4340 Steel ^[11]	0.25	0.160	0.038	0.0158	129	~ 1	-	RT	N	170

+ Derived from^[13]

V V-notchRT Room Temperature

N Notch

* Using static calibration^[9]FC Fatigue Crack

Table 3.2 Comparison of periods of oscillation for 3 point bend geometry

Turner's^[12]
model
$$\gamma = 2 \pi \left[\frac{CSBW_e}{k_n(1+k_p/k_e)} \right]^{\frac{1}{2}}$$

Ireland^[13](ASTM)*
$$\gamma = 1.68 \left[\frac{SWEBC_{LL}}{C_o^2} \right]^{\frac{1}{2}} = 1.68 \left[\frac{SBW_e}{k_n} \right]^{\frac{1}{2}}$$

McConnell and Server^[11](EPRI)*
$$\gamma = 3.36 \left[\frac{W^2EBC_{LL}}{C_o^2} \right]^{\frac{1}{2}} = 3.36 \left[\frac{BW^2_e}{k_n} \right]^{\frac{1}{2}}$$

* Derived experimentally

Table 3.3 Various loading rates for the 3 point bend geometry and associated interested authorities

$\dot{K}_I$ (MPa $\sqrt{m} s^{-1}$)				
0 <	< 2.5	Static	Conventional tension	BS5762 : 1979 BS5447 : 1977
2.5 <	< 10 ³	Quasistatic	Servo-controlled tension	UK (BS)
10 ³ <	< 10 ⁵	Low rate	Hydraulic, low speed impact	UK (BS) ASTM, ASK
10 ⁵ <	< 10 ⁶	Medium rate	Impact @ V ~ 5ms ⁻¹	EPRI, ASTM, ASK
10 ⁶ <	< 10 ⁷	Med/High rate	High speed impact	-
10 ⁷ <	< 10 ⁹	High Rate	Stress Wave loading	-

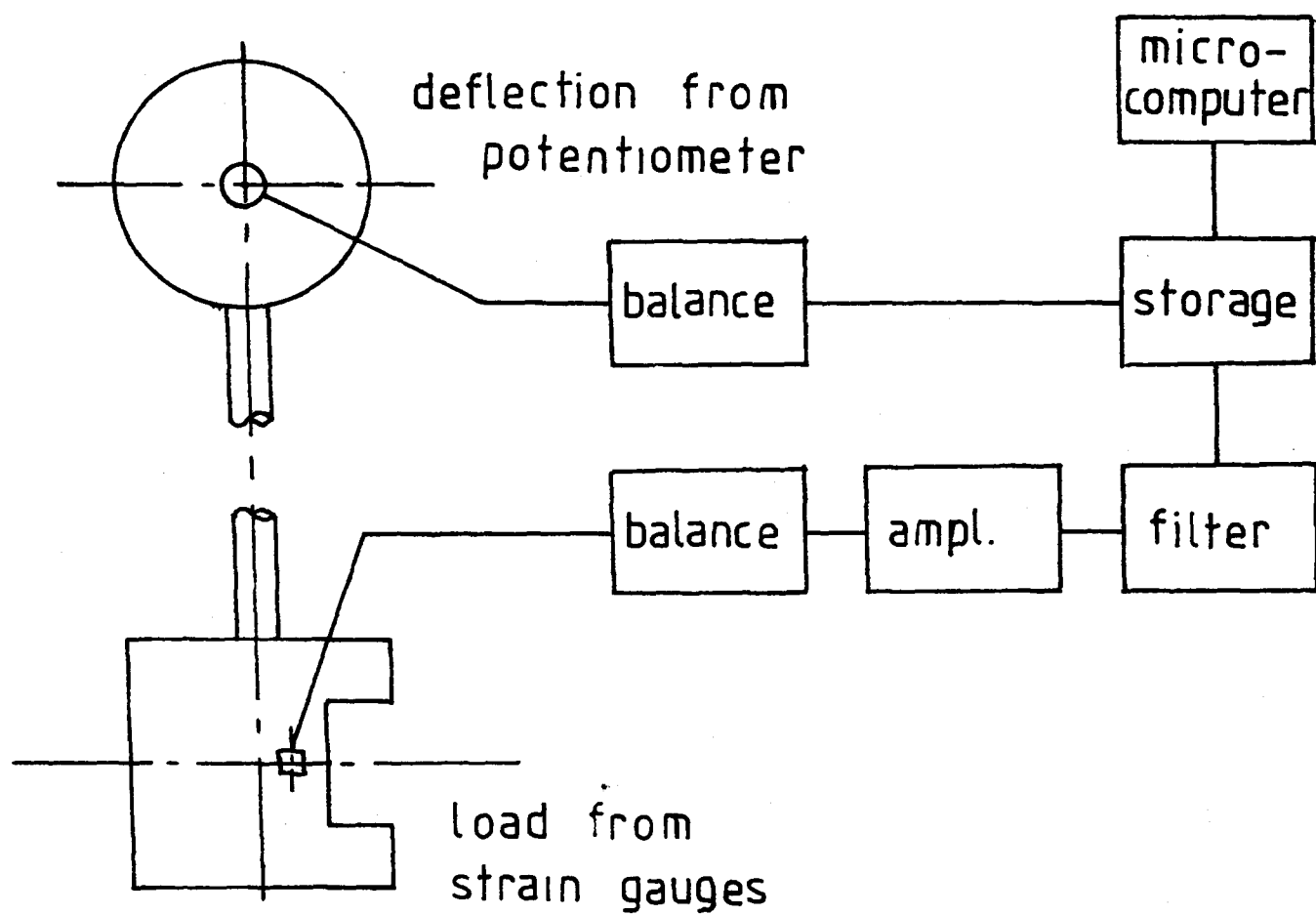


Fig. 3.1 Typical arrangement of an Instrumented Charpy testing facility.

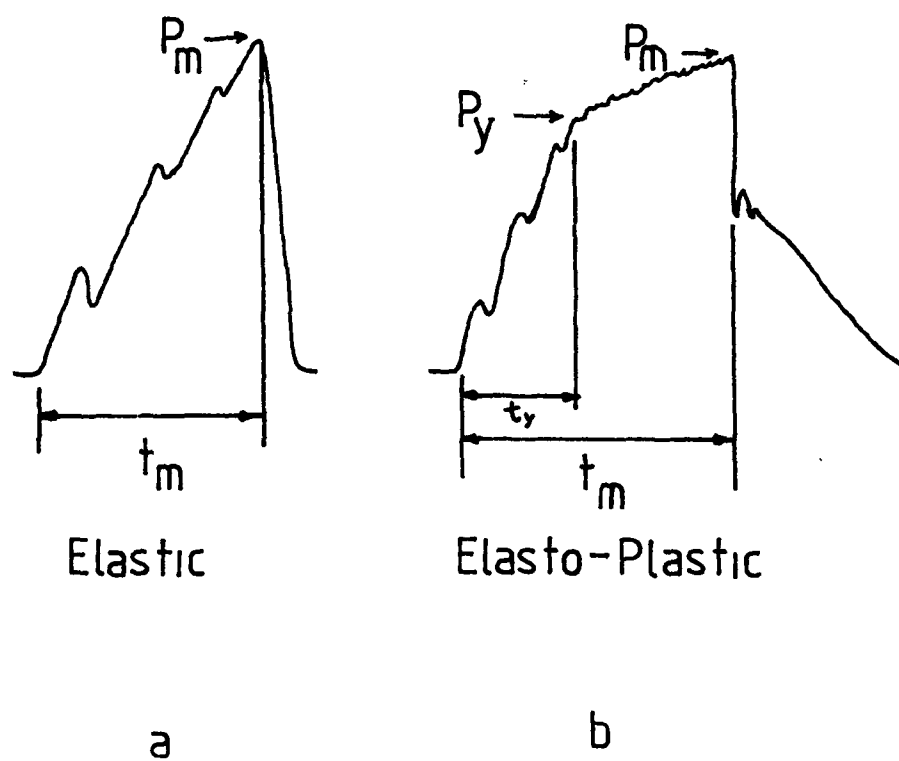


Fig. 3.2 Idealized $P(t)$ curves for elastic and elasto-plastic responses[3].

Fig. 3.3 K_{Id} v. $\log \dot{K}_I$ plot using Server's proposed methodology[33].

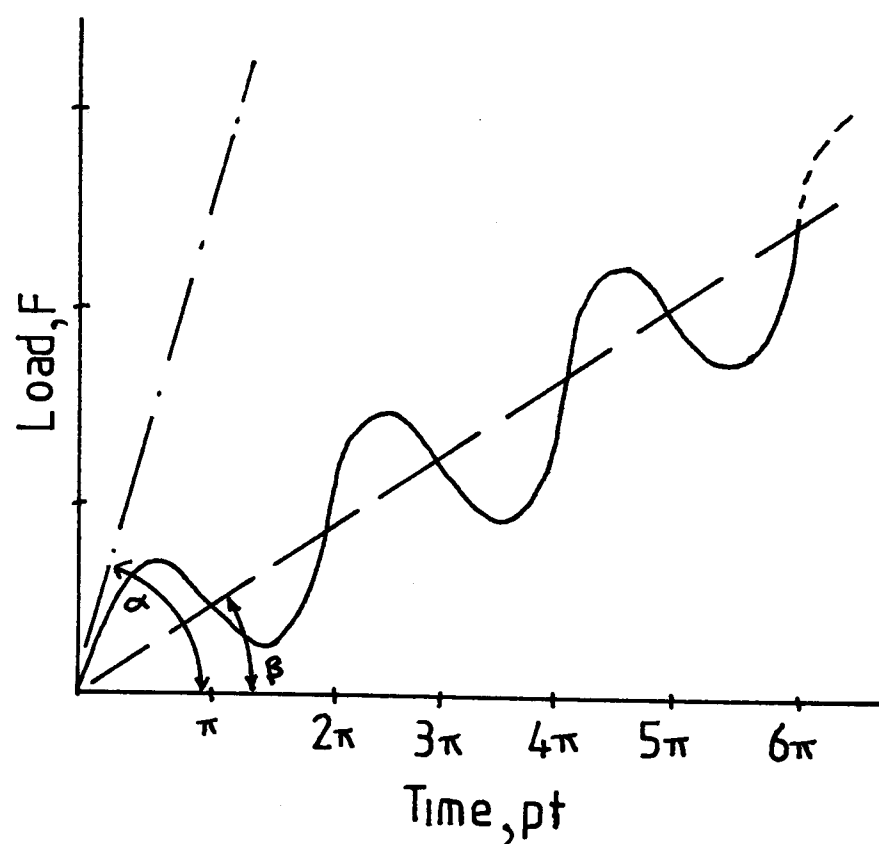
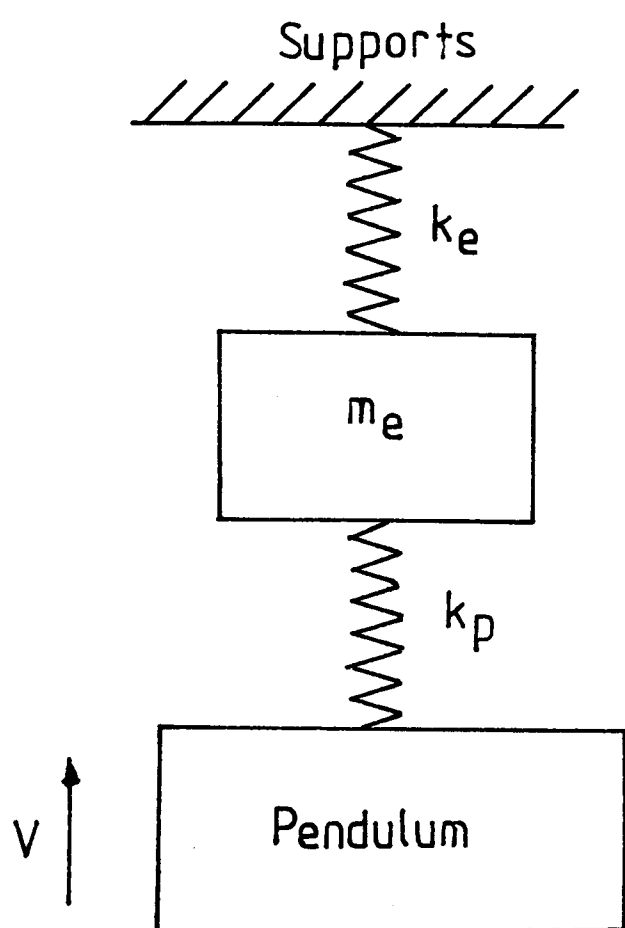
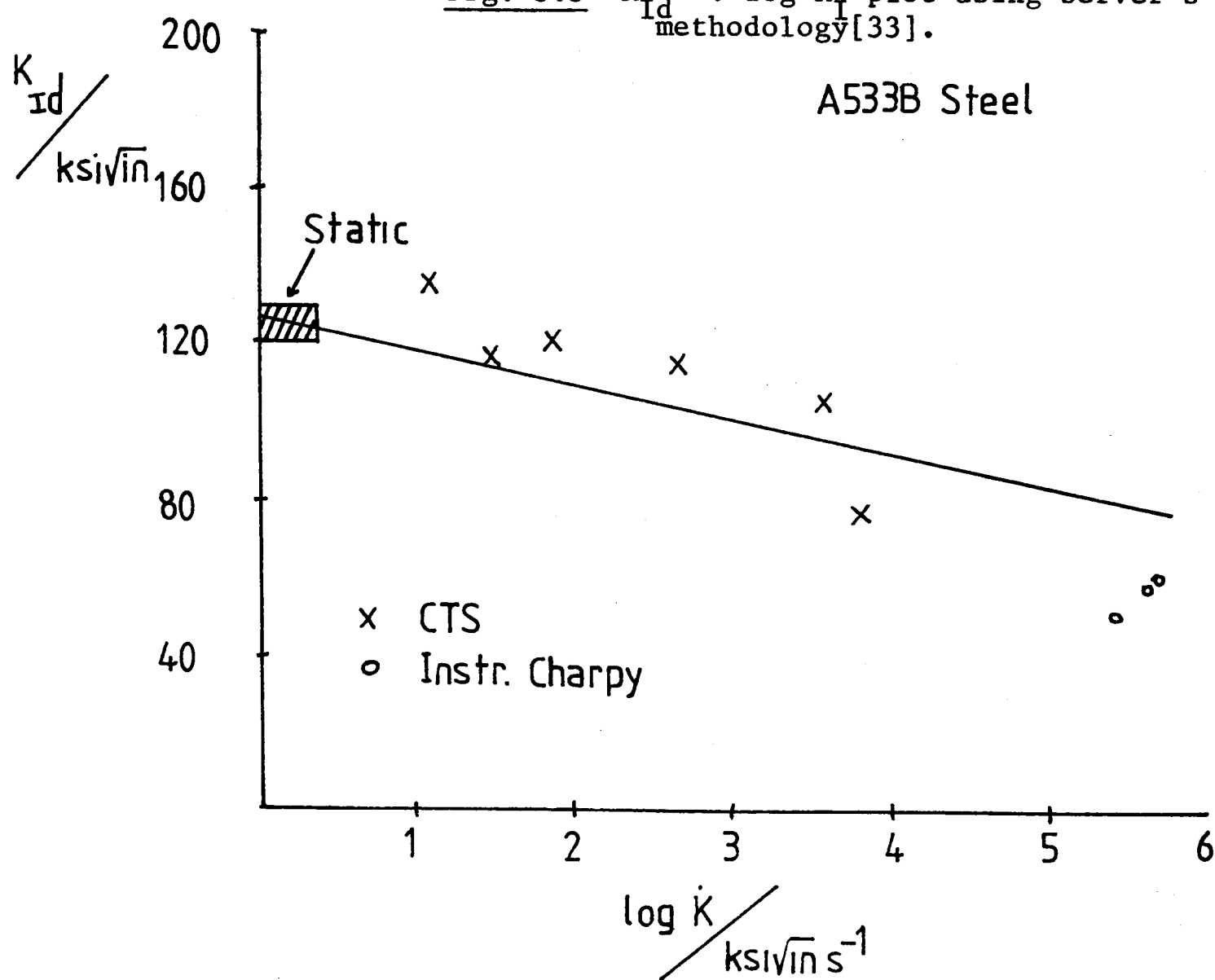


Fig. 3.4 Two degree of freedom model and results[12].

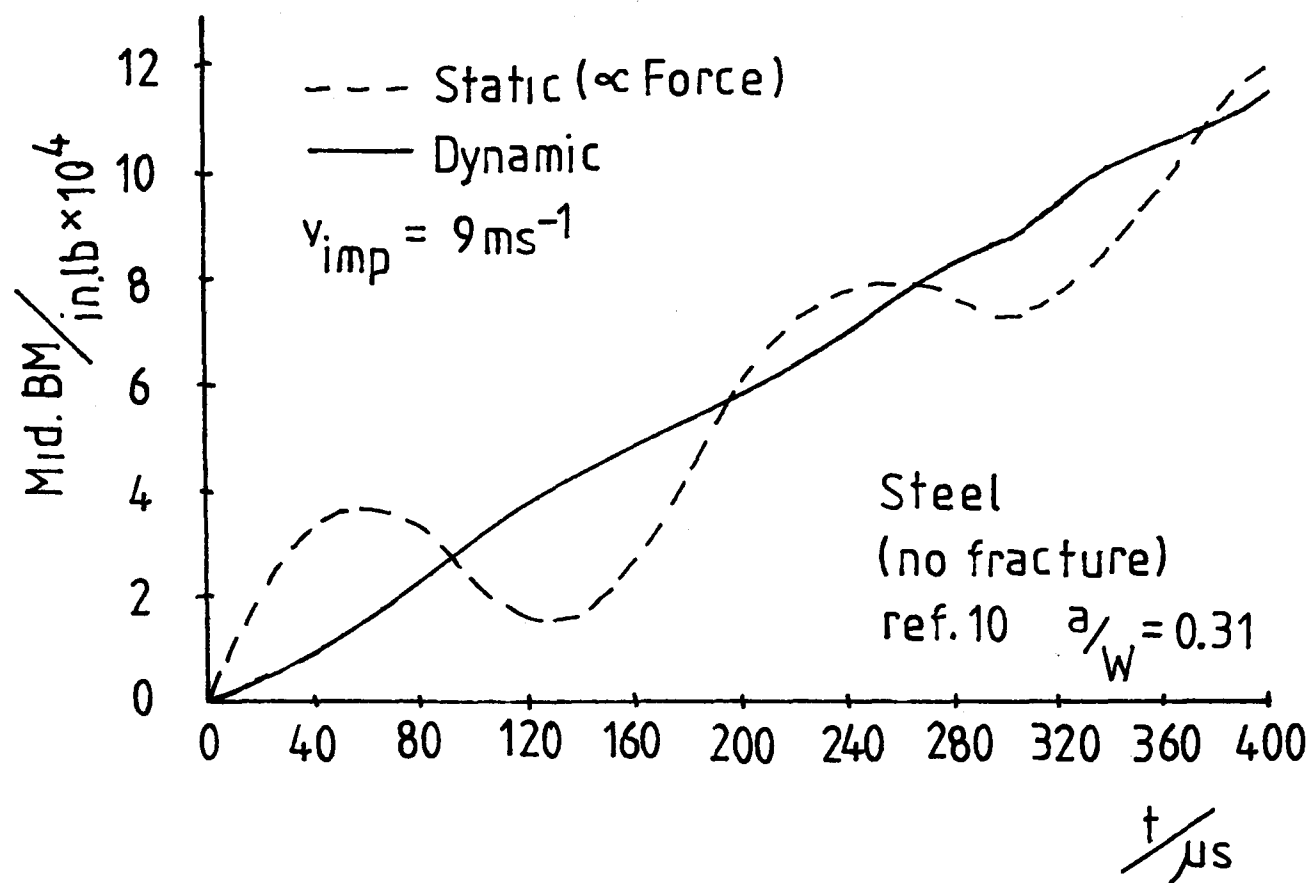


Fig. 3.5 Comparison of $P(t)$ and Dynamic Bending Moment [10].

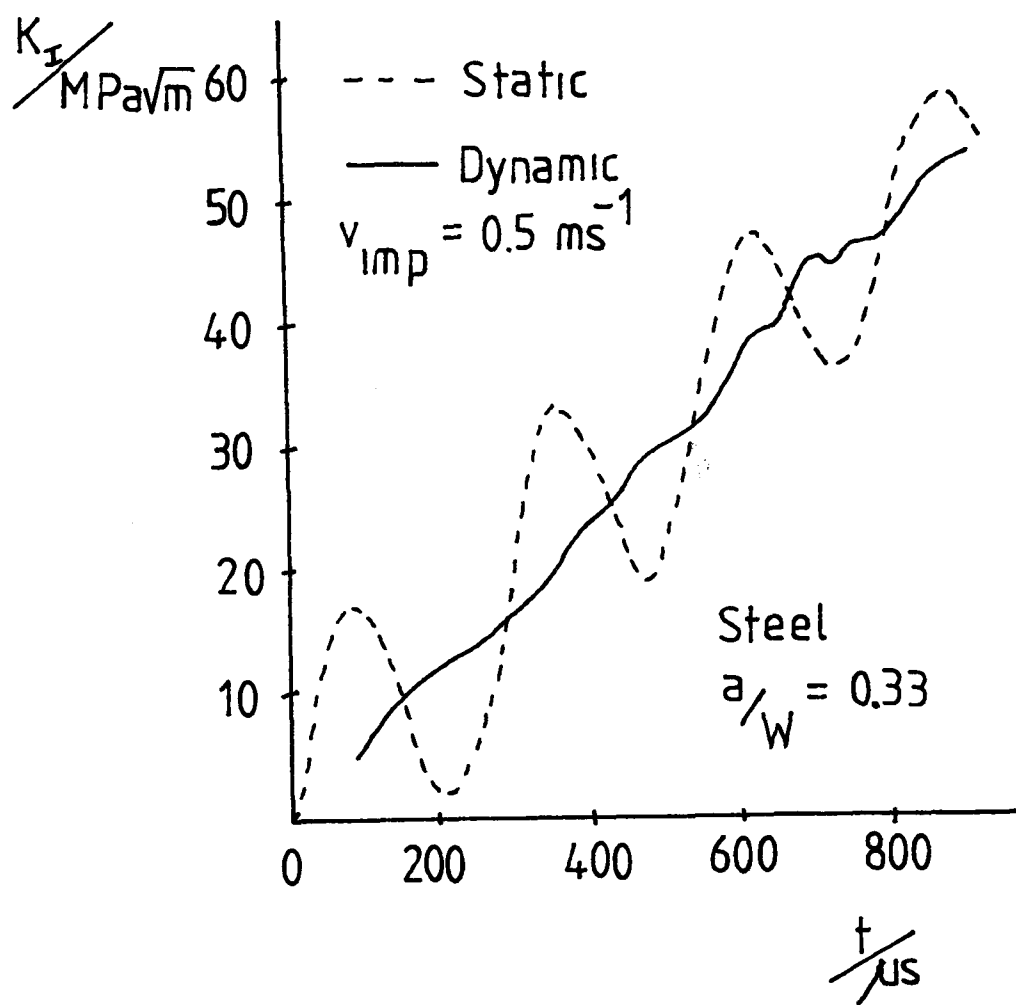


Fig. 3.6 Comparison of $P(t)$ and Dynamic Stress Intensity Factor [31].

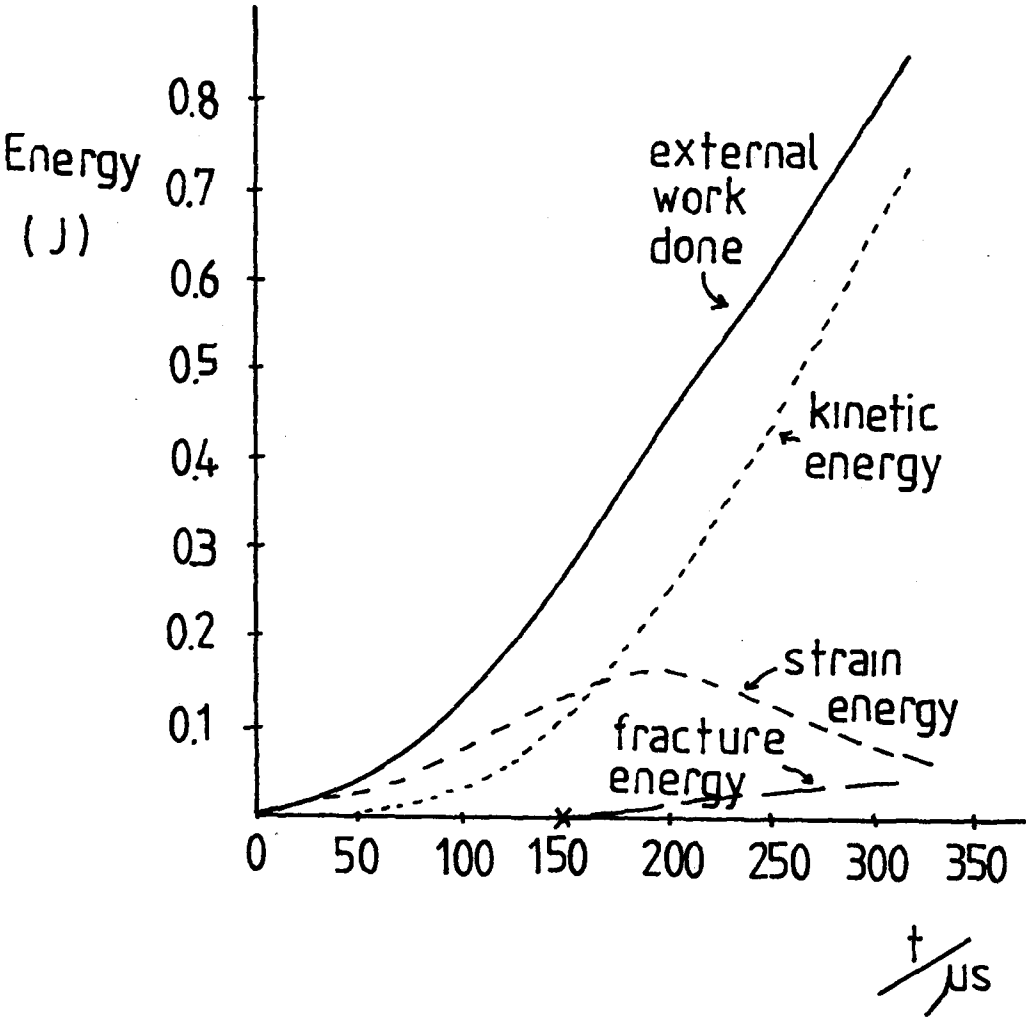


Fig. 3.7 Energy partition for the Charpy test[9].

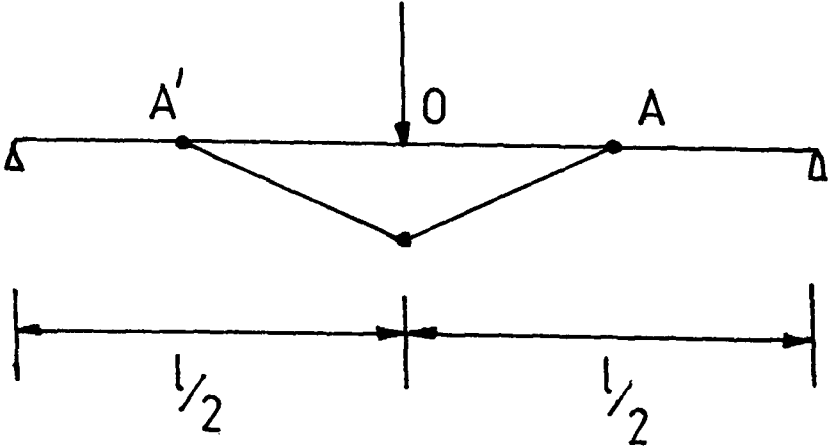


Fig. 3.8 Plastic beam model for the Charpy Test[8].

Fig. 3.9 Specimen and loading arrangement for SEN apparatus[31,32].

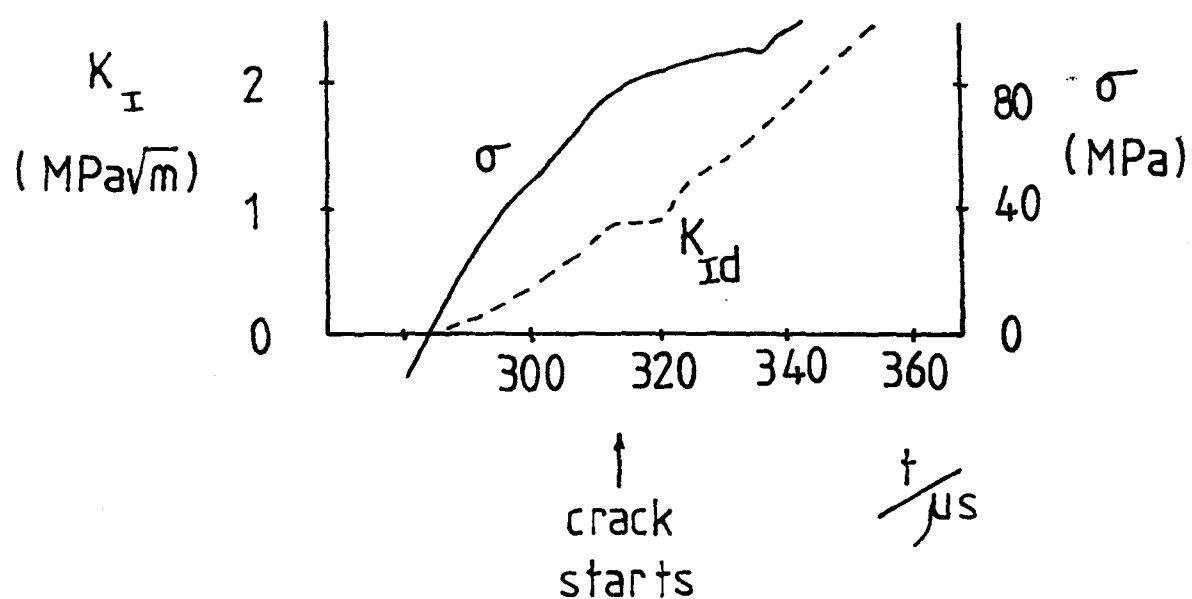
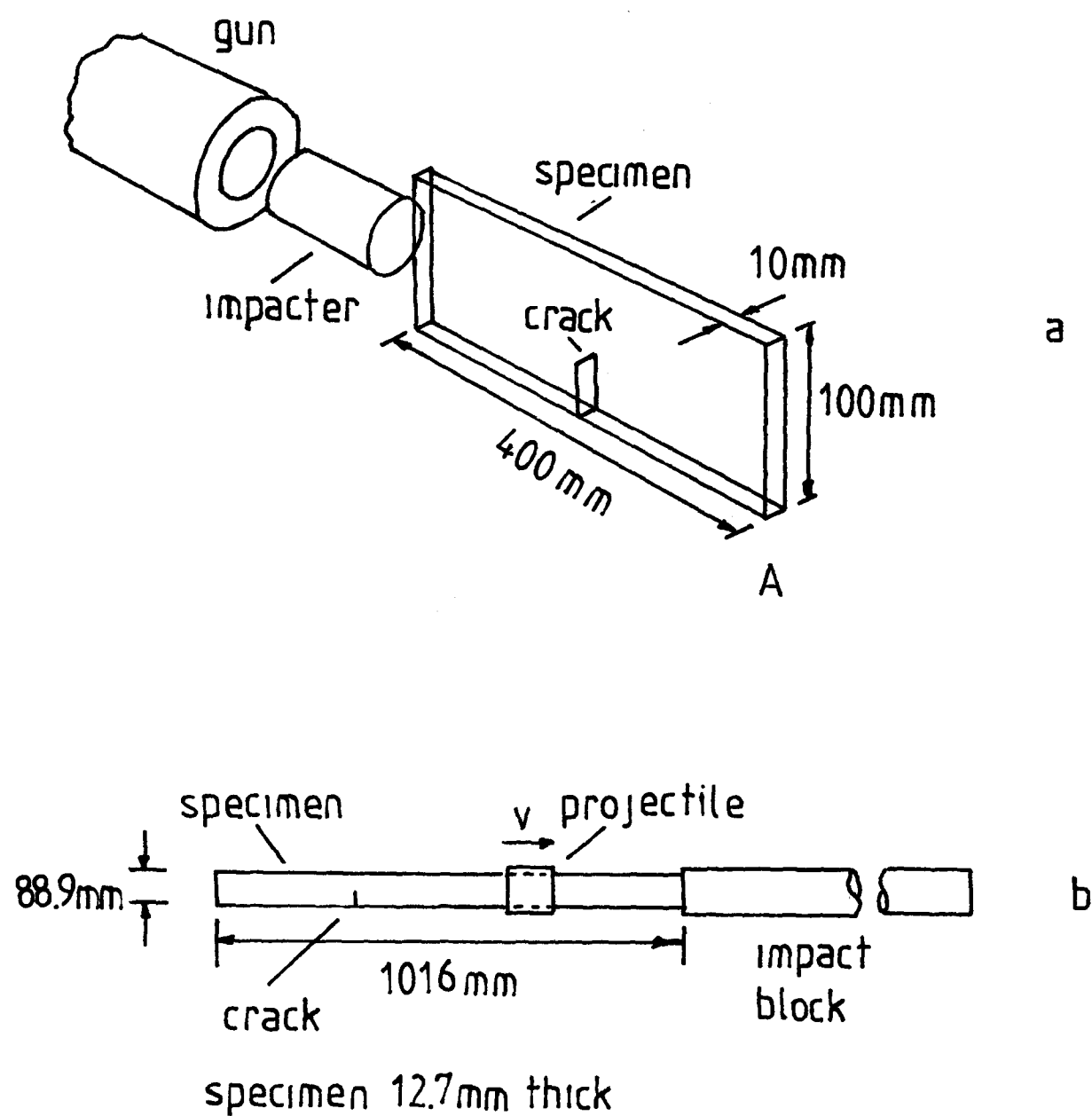


Fig. 3.10 Applied stress and resultant stress intensity factor for Araldite SEN tests[31].

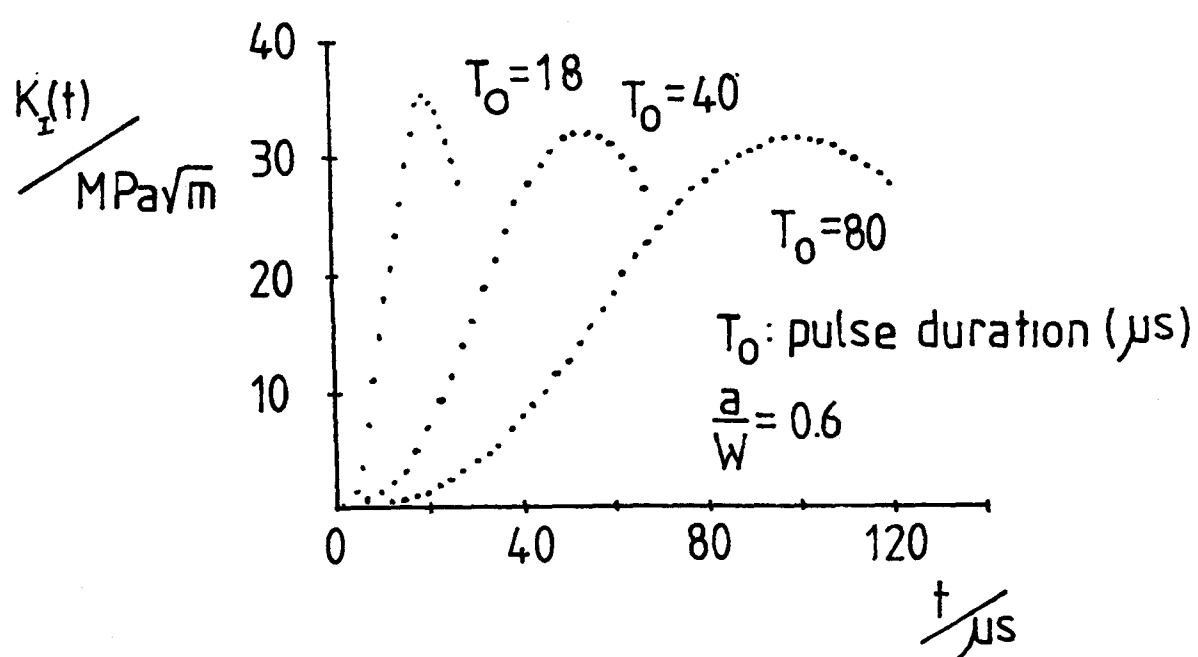


Fig. 3.11 $K(t)$ plots for various pulse loadings for 4340 steel SEN tests[32].

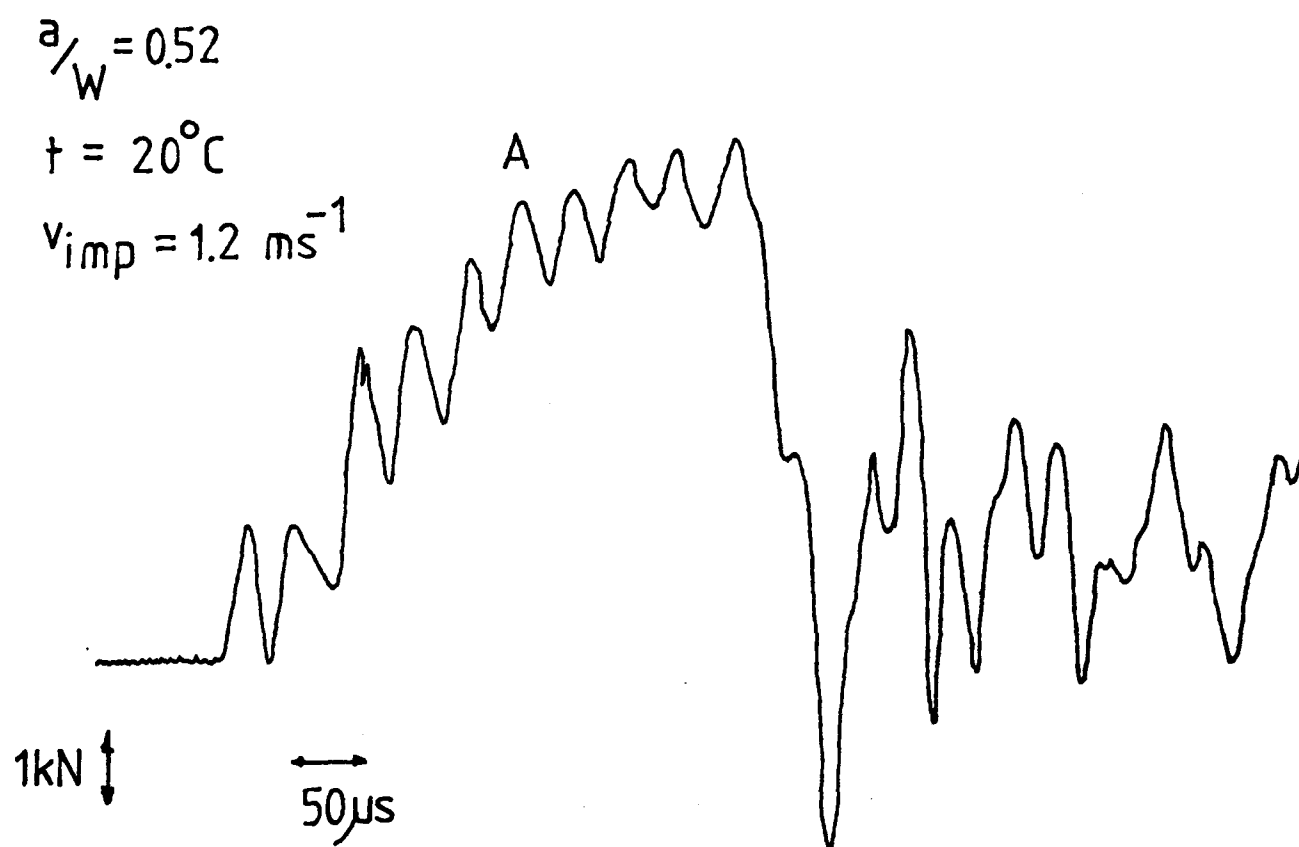


Fig. 3.12 A typical load trace from an Instrumented Charpy Test.

CHAPTER IV

THE DYNAMIC PHOTOELASTIC BEHAVIOUR OF 4 NOTCHED GEOMETRIES[9,10]

4.1 INTRODUCTION

From the literature survey, it has been shown that there is a requirement to consider the dynamic *calibration* of the Charpy geometry. It has also been shown that the dynamic behaviour of such a geometry is complex and so there is a further requirement to look for a geometry that gives rise to a more straightforward dynamic behaviour.

In studying the dynamic behaviour of various notched geometries complexities are reduced to a minimum by studying only the elastodynamic behaviour for a stationary notch or crack. Thus difficulties associated with inelasticity and crack propagation are avoided.

An experimental technique that has been used extensively for elastodynamic problems is dynamic photoelasticity and this method has already been discussed (see Section 2.4.2.2). The usual model material used is an epoxy, e.g. Araldite, but such a material displays high stress wave speeds, i.e. 2 kms^{-1} . Thus to capture these high speed fringes a camera capable of 10^5 frames per second is required. Such a facility is complex and expensive and it was decided, for this exploratory work, to use a low modulus birefringent material. The maximum stress wave speed of the urethane rubber 'Photoflex', manufactured by Sharples Ltd.^[11], is of the order of 0.2 kms^{-1} and hence photography at 10^4 frames per second can be considered. Such photography can be achieved using a high speed ciné camera, and hence the experimental technique is simpler. One problem with using a low modulus photoelastic material is that it is very

rate sensitive - E can increase by 100% from static to high strain rates^[1]. Thus the results given in this Chapter have to be regarded as purely qualitative.

4.2 TEST MATERIALS, SPECIMENS AND TECHNIQUE

The properties of the urethane rubber 'Photoflex' are given in Table 4.1. From photographs of fringe patterns it is estimated that E , under impact loading, rises to about 40 MPa.

Four specimen types were tested, namely Charpy 'V' notch, Izod, Slender Cantilever and Double Notched Bar in tension. The dimensions are given in Fig. 4.1. The notches were cut as a 45° 'V' using a scalpel blade. The specimens themselves were cut from 3 mm thick precast sheet using the same technique.

The specimens were mounted on a perspex frame in a diffused light polariscope and loaded by a falling weight or pendulum of 200 g mass falling from a maximum height of 650 mm against a Tufnol tup (see Figs. 4.2 and 4.3). Fig. 4.4 shows the method for dropping the weight and for monitoring the performance of the apparatus. The stroboscope was set to flash at a frequency of 8000 flashes per second for 0.25 s, the energy per flash being 2J. The sharpness of the monochromatic image was improved by fitting a dark green filter (Kodak Wratten No. 58) to the camera lens. The filter was chosen, after several trials, as a satisfactory compromise between the two requirements of eliminating unwanted interference between complementary colours in the higher order fringes without cutting out too much light. Table 4.2 gives the sequence of events in a test. The transient recorder shown in Fig. 4.4 monitored the time taken from camera start to the instant when the weight hit the tup. A quartz load cell was placed between the tup and the impacting weight and the load trace captured by the transient recorder. Typical results are given in

Fig. 4.5. The form of the load pulse is approximately triangular for the three bending tests, while in the double notched bar a rapidly rising load remains at roughly fixed level for a significant proportion of the total time.

The film used in the high speed ciné camera was 16 mm FP4 in 200 ft. lengths. Fringe patterns were therefore enlarged to give a 5" x 4" print for examination. This fact coupled with the blurring of high order fringes due to the material itself meant that prints were of poor quality (see Fig. 4.6). The polariscope was set up for dark field with quarter wave plates hence isochromatic fringes resulted. At a film speed of 8000 fps one frame occurs every 125 μ s and fringes can therefore be tracked as stress waves travel back and forth in the specimens.

4.3 RESULTS

The main interest centres around behaviour after short times, i.e. we are interested in stress wave loading rather than inertial loading. Thus stress waves emanate from the point of impact, reflect from free surfaces, interact with the notched region and give rise to a growth of stress intensity at the notch tip. After a number of wave reflections the loading and stress distribution becomes inertial in character. The interpretation of the photoelastic fringes recorded on film is facilitated by plotting fringe centre lines and establishing their order.

4.3.1 Charpy Specimen (Fig. 4.7)

Stress waves, originating from the point of impact, propagate in a symmetrical pattern, until they reach the notch tip (Figs. 4.7a and b). At that instant they are reflected: the stress field at the notch is essentially tensile while at the loading side it is compressive. The absence of mode I fringes, i.e. of symmetrical closed

loops at either side of the notch, will be noticed. The pattern observed in frame 2, produced by the interaction between the waves initiated at the loading point and the waves diffracted at the notch tip, remains virtually unchanged until frame 7, with small mode I loops gradually developing after frame 4 (Fig. 4.7b and c). At frame 7 (Fig. 4.7d) well developed mode I loops are clearly visible. This coincides with the time when flexural waves have reached the supports and the specimen begins to bend as a beam under the combined effect of a central point load and a distributed load due to its own inertia. As bending becomes more pronounced (frame 8, Fig. 4.7e) the K_I mode loops grow.

It is possible to distinguish three phases in the deformation of the Charpy specimen. The first phase is governed by the interaction between original and diffracted waves (frames 1, 2, 3). In the second phase, flexural waves start to move outwards towards the supports (frames 4, 5). In the third phase, after frame 6, the flexural waves cause a tensile loading of mode I at the notch. The stress distribution during the first two phases is essentially different from that of the freely supported beam with a central point load, assumed for the derivation of the static *vibration* of the specimen. A calibration based on the static *calibration* would therefore appear to be entirely meaningless. In the third phase, it could be argued that a quasistatic condition is achieved as a result of multiple wave reflections. It is, indeed, true that frame 8, for example, shows a clear bending behaviour, combined of course with shear. There is, however, an important difference between frame 8 and the static pattern reproduced in Fig. 4.7f, a difference that results from the inertial loading. The static pattern indicates a high stress gradient around the central point load that does not exist in the dynamic case, while the stress distribution on sections intermediate between the supports

and the notch also differs markedly. The categorization of the response of the Charpy Specimen into three phases is summarized in Table 4.3.

4.3.2 Izod Specimen (Fig. 4.8)

At first, waves originate from the loading point and travel until they reach the notch, which is now level with the support of the cantilever specimen (Fig. 4.8a and b). As soon as the waves reach the notch, a mode I loop forms at the tip (Fig. 4.8c). From that instant the fringe pattern remains virtually the same, with growth of the mode I loops while bending, caused by inertial forces, increases. At frame 8 (Fig. 4.8e) the tup loses contact with the specimen, bending is solely a result of distributed inertial loading. Not unlike the Charpy, the Izod specimen tested under impact never develops a stress distribution equal to the one existing under static conditions, as shown by a comparison between Fig. 4.7e and f.

4.3.3 Slender Cantilever Specimen (Fig. 4.9)

In both the Charpy and Izod specimens the aspect ratio is very low making the simplified 'strength of materials' beam theory inaccurate. A further criticism of the Izod specimen is that the notch is level with the support and any variation in the support condition would affect the stress distribution in this critical region. Both difficulties are avoided by using a slender cantilever beam with the notch away from the support and sufficiently remote from the point of application of the load to ensure mode I loading only at the notch tip. The first frame (Fig. 4.9a) is very similar to the same frame in the Izod specimen but as the flexural waves develop (Fig. 4.9b) bending is symmetrical about the neutral axis and the effect of shear is negligible. The slender cantilever now acts as a wave guide and only well formed flexural waves reach the notch. When

this happens a mode I loading results in the characteristic loops found when the notch tip is close to a free surface (Fig. 4.9c). These loops tend to elongate and tilt towards the notch axis, while still satisfying the Westergaard equation^[2]. After frame 6, the stress pattern remains unchanged, the specimen bends about the notched section through inertial loading. Eventually, a symmetrical pattern develops at that section (Fig. 4.9d) but even then, downstream of the notch, the specimen only sees a small load. In contrast, the static situation gives a more evenly distributed load between the parts of the specimen.

4.3.4 Double Notched Bar (DNB) (Fig. 4.10)

Disregarding the first frame, in which the influence of self weight masks the dynamic loading, approximately plane wave fronts corresponding to fringe orders 1, 2 and 3, are shown to travel from the loading end, in Fig. 4.10a. As soon as the first wave front crosses the notched section, mode I loops form at the notch tips. Due to the initial curvature and slope of the wave front, there is some out-of-phase between the waves diffracted by the notches. The pattern also differs between the upstream and downstream sides (Fig. 4.10b). Mode I loops grow with time (Fig. 4.10c) and as a result of successive reflections at the ends, a symmetrical loading pattern ultimately develops (Fig. 4.10d). This pattern is identical to the one obtained under static loading.

4.4 DISCUSSION OF RESULTS

The difference between static and dynamic stress fields can be categorized into two effects: namely the dependence of material properties on strain rate and the inertia of the material^[3]. These effects are very large, and probably non-linear, for the low modulus

urethane rubber. Thus conclusions from this experimental work are purely descriptive and cannot be too closely related to the linear elastic behaviour of Araldite or metals.

Use of a flexible polyurethane meant that fracture of specimens could not be observed, thus the point of fracture has to be postulated for each test. For the Charpy specimen fracture could occur in phases 1 and 2 (see Table 4.3), i.e. before the supports take effect. In this case a dynamic compliance would be required. If, on the other hand, fracture initiated in phase 3, the material in the vicinity of the notch tip would have already been affected by stress wave conditioning, due to wave action during phases 1 and 2, thus fracture could be a result of a stress history effect. Application of Server's criterion for reliable load and time evaluation^[4], i.e. guaranteeing that specimen inertial oscillations has subsided, means that fracture should occur after frame 30, approximately. It has been shown that, for plastics and metals, this restriction is too conservative^[5,6] and that crack initiation occurs well before this time.

Another requirement in Server's test specification is that the total available energy at impact should be larger than three times the energy dissipated at maximum load. Thus the kinetic energy of the impacting tup should ensure that its velocity is not reduced by more than 20% during the tests. In these experiments this condition was not met and, in any case, fracture did not occur. But it has been shown that the static solution of a point load applied to the beam is never achieved in these experiments due to the inertia of the specimen giving rise to a differing load distribution. Thus in such a case the static *calibration* cannot be used, even after large time.

In Section 3.6.3 Loss' procedure was described in which a strain gauge is positioned adjacent to the crack tip to monitor the mid span load and to reduce inertia effects to a minimum. Ireland^[7] has made

an extensive study of the relation between position of the strain gauge on the impactor, specimen and support and the load applied to the specimen. For example, a strain gauge at quarter span on the side of the specimen (see Fig. 4.11) would monitor the flexural response, and would thus be a measure of mode I loading at the notch tip. Figure 4.11 gives plots of fringe order v. frame number for various strain gauge positions. It can be seen that the growth of fringes yields points of inflexions at the time when the supports take effect, i.e. transition from phase 2 to 3. This point of inflexion occurs during the development of mode I loops. After frame 7 there is a steady growth of fringe order at strain gauge positions due to the inertial loading of the beam.

It has been shown in Chapter III that *calibration* functions, whether static or dynamic, based on strain gauge signals often give conflicting answers, but strain gauging does give an indication of the stress level in the specimen and provides a useful way of comparing different geometries.

The maximum loading rate, as defined by $\dot{K}_I = K_{Ic}/t_f$ where t_f = time from loading at notch tip to fracture, associated with the Charpy specimen is limited to $10^5 \text{ MPa m}^{\frac{1}{2}} \text{ s}^{-1}$ [8]. This is due to the fact that stress wave loading is predominantly shear and flexure. In the Izod and, especially, the slender cantilever this upper limit could be further reduced. Thus problems could be encountered with these geometries in achieving higher strain rates. The two main reasons for studying the cantilever geometry were that mode I response is marked and the geometry is more amenable, as compared with the Charpy specimen, to a dynamic strength of materials analysis.

Strain gauge positions, in the case of the Izod specimen, were taken at half span and adjacent to the notch tip (see inset Fig. 4.12). Fringe order growth was shown to be oscillatory at half span,

whereas a steady growth occurred in fringe order adjacent to the crack tip (see Fig. 4.12). In the case of the slender cantilever strain gauges cannot be positioned adjacent to the notch tip, but a strain gauge positioned at the back of the notch gave a steadily increasing curve (see Fig. 4.13). The effect of the slenderness of the cantilever, i.e. of the vicinity of the free surface to the notch, makes a strength of materials analysis difficult. Thus there is scope to optimize a geometry between the low aspect ratio Izod specimen and the slender cantilever. Another aspect of the slender cantilever is the large deflections associated with the response, and hence possible non-linear behaviour.

The Double Notched Bar (DNB) was adapted from the circularly grooved bar tests described in Section 2.5.5. Although the model used here is plane, it is expected that its response will be very similar to a diametral slice in a circular bar. From the fringe diagram of Fig. 4.10 it has been shown that a mode I response occurs immediately after incidence of P waves. The growth of fringe order with frame number for various strain gauge positions is given in Fig. 4.14. Steadily rising curves occur with growth in fringe order at the end of the specimen. The dynamic response of this geometry depends on the length of the specimen and the reflection of P waves from the ends. It should be noted that the dynamic response of the geometry tends to the static response after large time, thus the static *vibration* can conceivably be used for this geometry.

Other possible geometries that we could have considered are compact tension specimen, single edge notched specimen, double cantilever beam specimen and other, even more complex, shapes. Given the technological orientation of the research, it was decided to concentrate on the widely used fracture toughness tests.

4.5 CONCLUSIONS

Of the four configurations examined, the Charpy does offer considerably greater difficulties for the interpretation of test data than the other three. The Izod and the slender cantilever offer greater potential for the quantitative determination of fracture toughness: testing is no more difficult than for Charpy specimens and the interpretation of results is simpler, particularly for the slender cantilever. The simplest geometry is the double notched bar, but like in the other cases, the use of a static *calibration* has not been proven.

The loading systems employed in these tests do not correspond exactly to those used in practice for both the Izod and Charpy test. They do, however, give good indications of how the specimen responds to an impact load of the type experienced in the actual test.

The next step is to look at the response of various notched geometries under more accurate conditions, and to this end an epoxy model material with 10^5 fps photography needs to be used. Given the complexity of the associated experimental techniques two geometries, only, were selected for further study. These were the Charpy geometry, given its wide use, and the Double Notch Bar, as this geometry would seem to display the most straightforward response under dynamic conditions.

4.6 REFERENCES, TABLES, FIGURES

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E (Static)	0.7 to 4.1 MPa
ρ	900 kg/m ⁻³
Poisson's Ratio	0.48
Stress Fringe Value (Static)	0.14 - 0.17 kNm ⁻¹

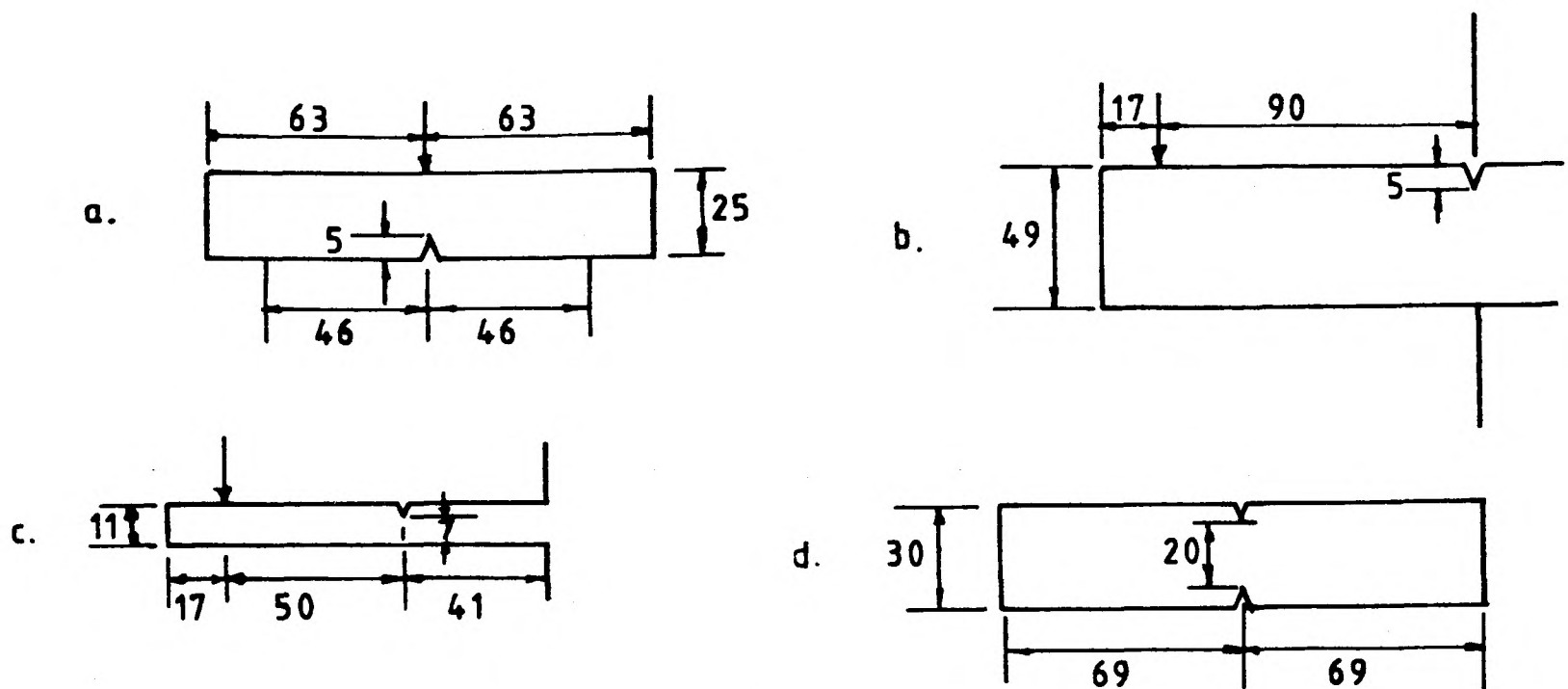
Table 4.1 Properties of 'Photoflex'

Time(s)	Event
0	Start switch, camera on, relay A on, time delay relay B on.
0.625	Weight released.
1.00	Weight cuts photoelectric cell, strobe on.
1.01	Camera up to speed.
1.06	Weight hits tup.
1.25	Strobe off.
1.51	Camera off.

Table 4.2 Sequence of Events in a Test (see Fig. 4.4)

Phase	Description
1	Localized dilatation and shear waves.
2	Flexural waves travelling to the supports
3	The specimen bending as a beam (inertial loading).

Table 4.3 Categorization of the Response
of the Charpy Geometry



Dim. in mms

Fig. 4.1 Dimensions of the 4 notched geometries:

- (a) Charpy
- (b) Izod
- (c) Slender Cantilever and
- (d) Double Notched Bar.

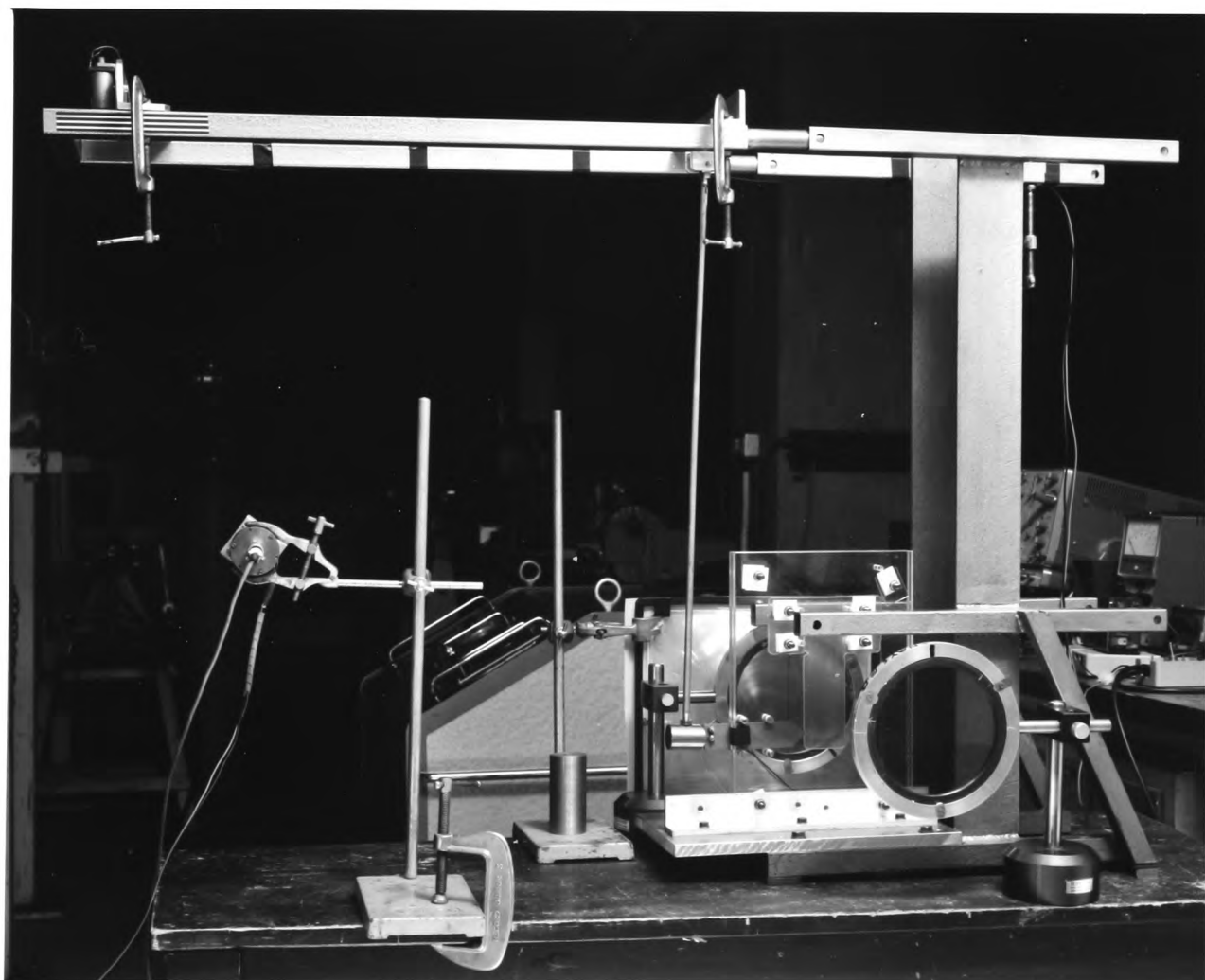


Fig. 4.2 General view of the loading frame for 'Photoflex' tests.

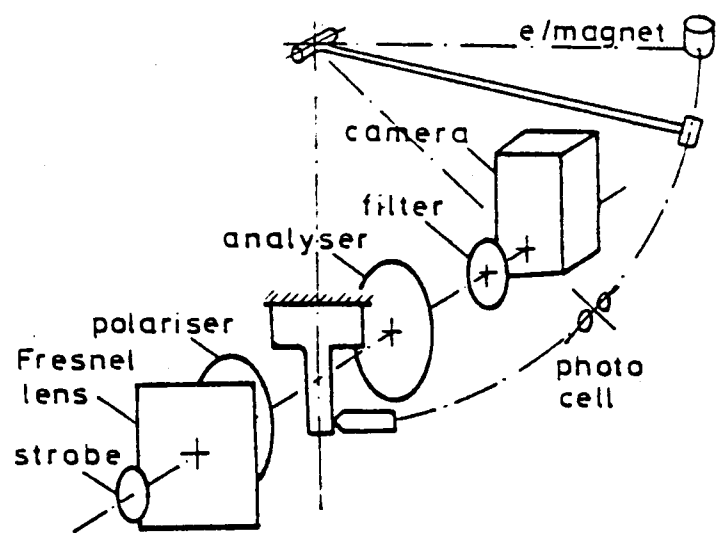


Fig. 4.3 Schematic of the loading frame and photoelastic bench for 'Photoflex' tests.

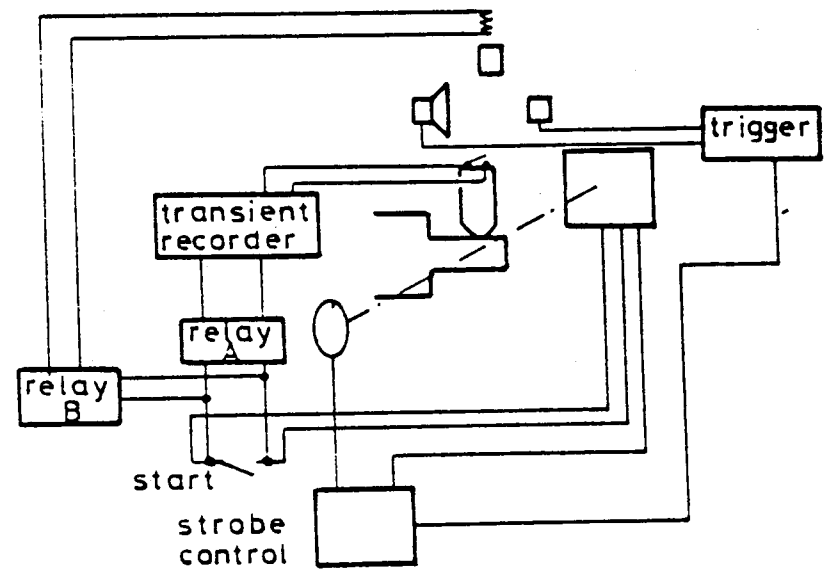
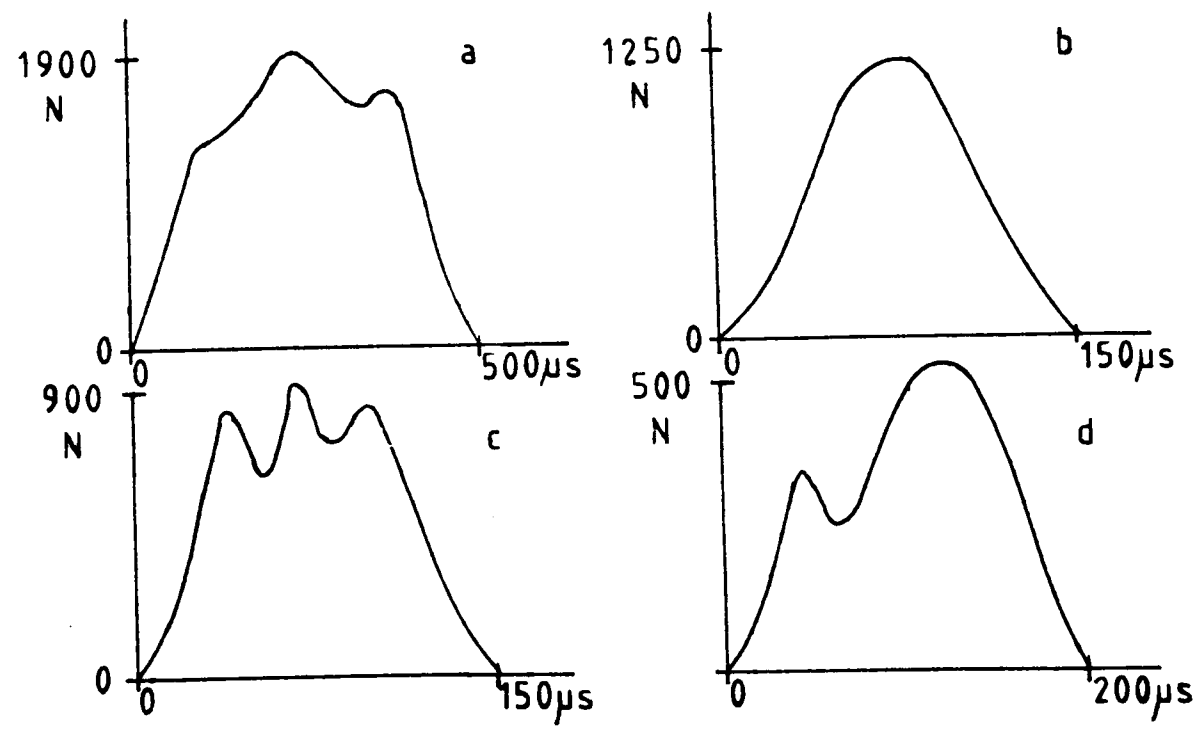


Fig. 4.4 Schematic of instrumentation and synchronization of events.

Fig. 4.5 Load-time pulse for:

- (a) Charpy
- (b) Izod
- (c) Slender Cantilever and
- (d) Double Notched Bar.



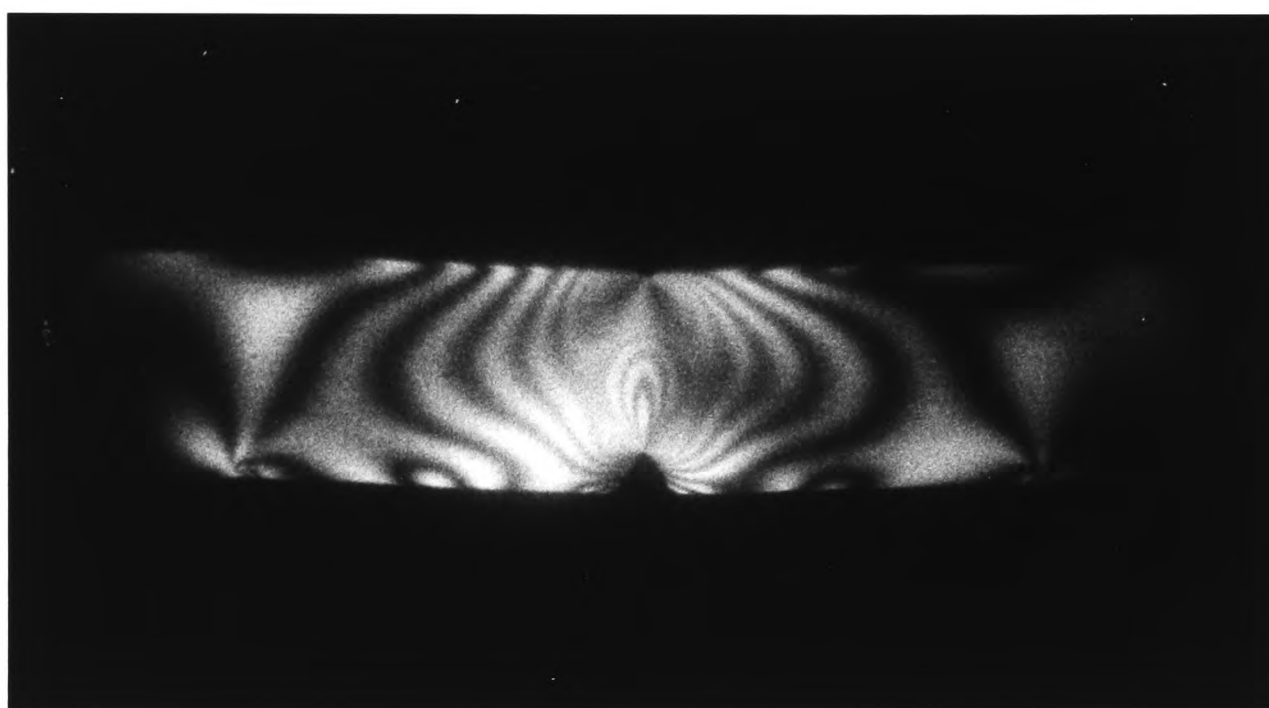


Fig. 4.6 Print of typical fringe pattern for the Charpy geometry.

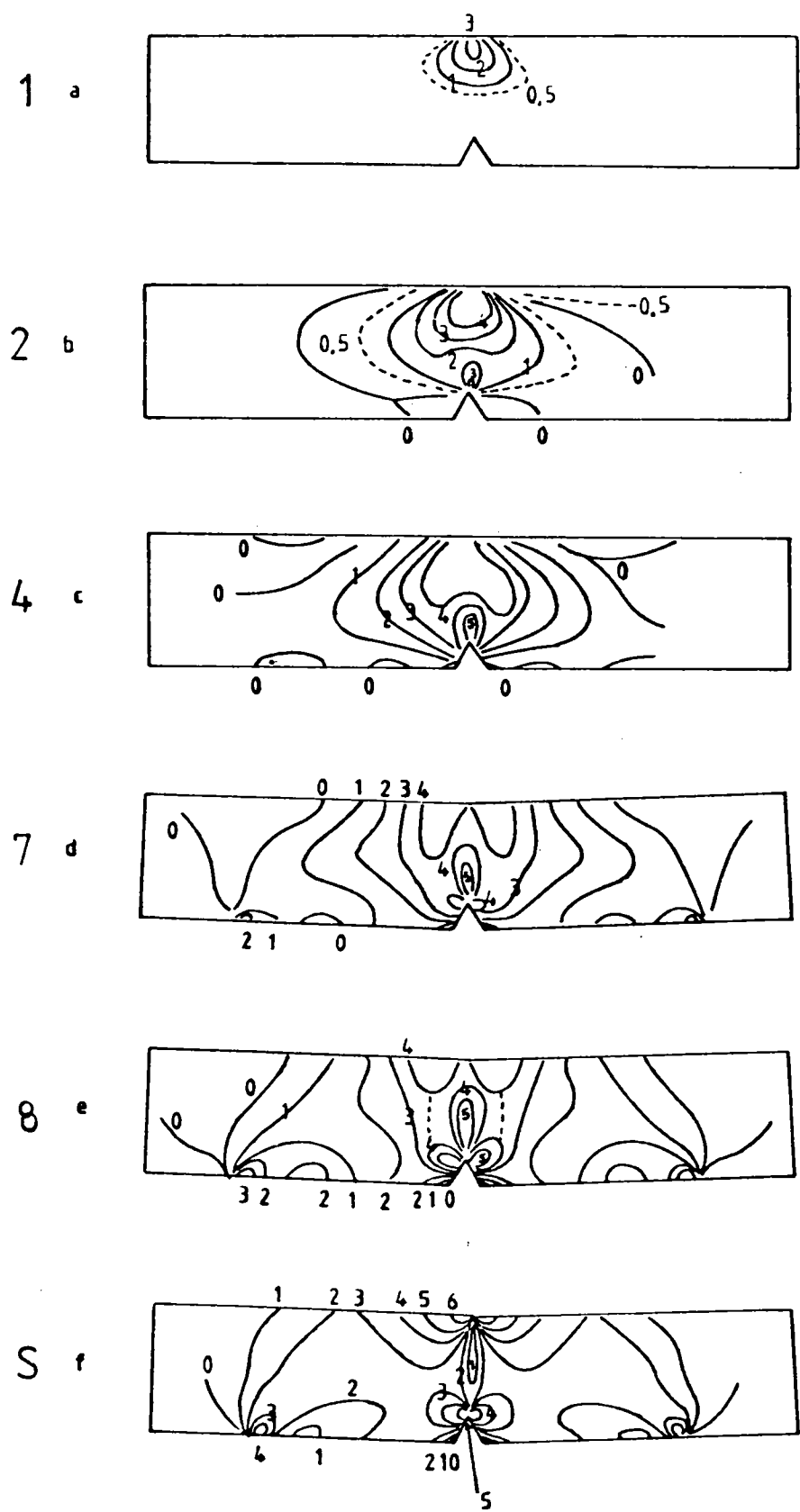


Fig. 4.7 Fringe pattern development for the Charpy specimen.

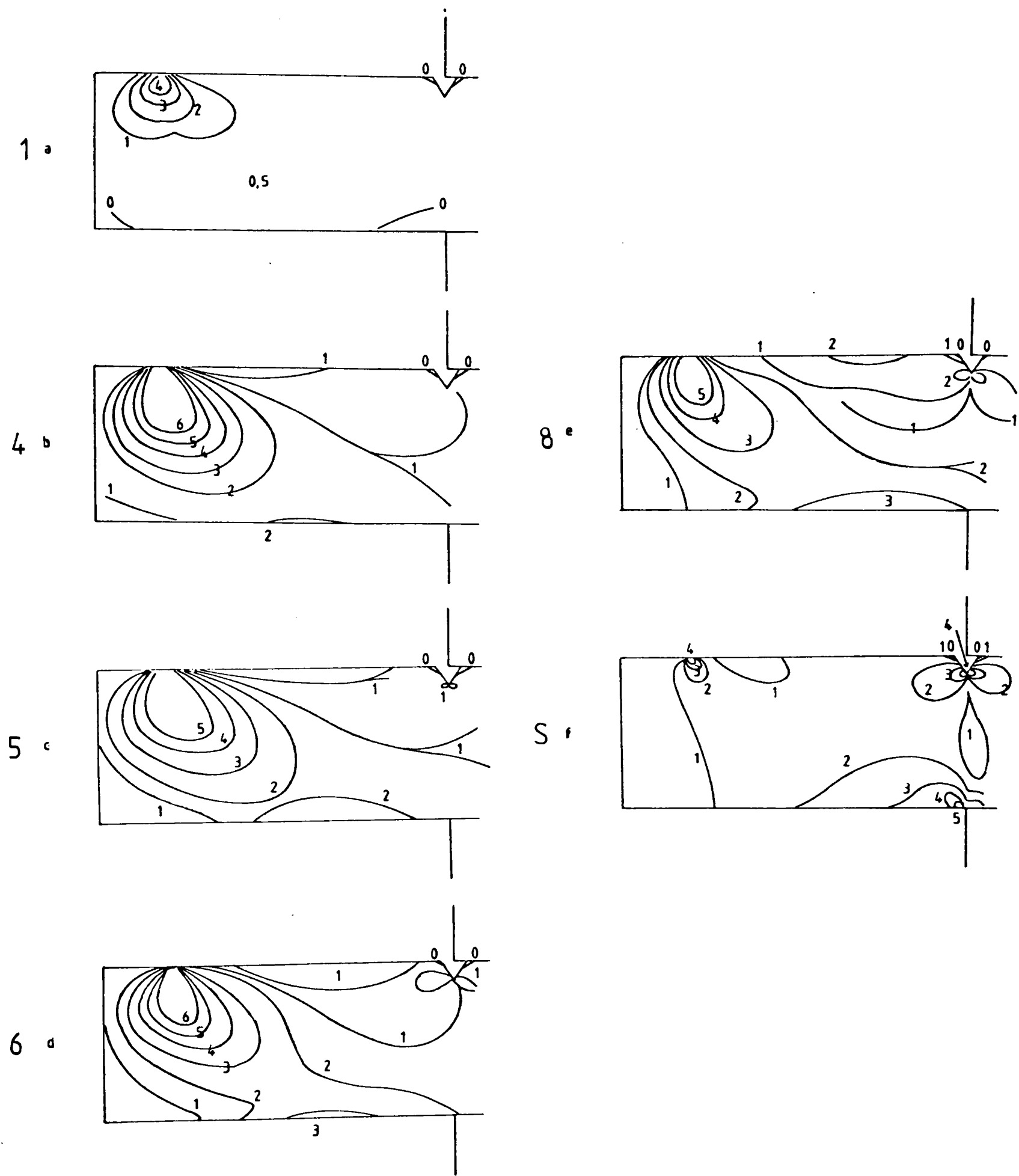


Fig. 4.8 Fringe pattern development for the Izod specimen.

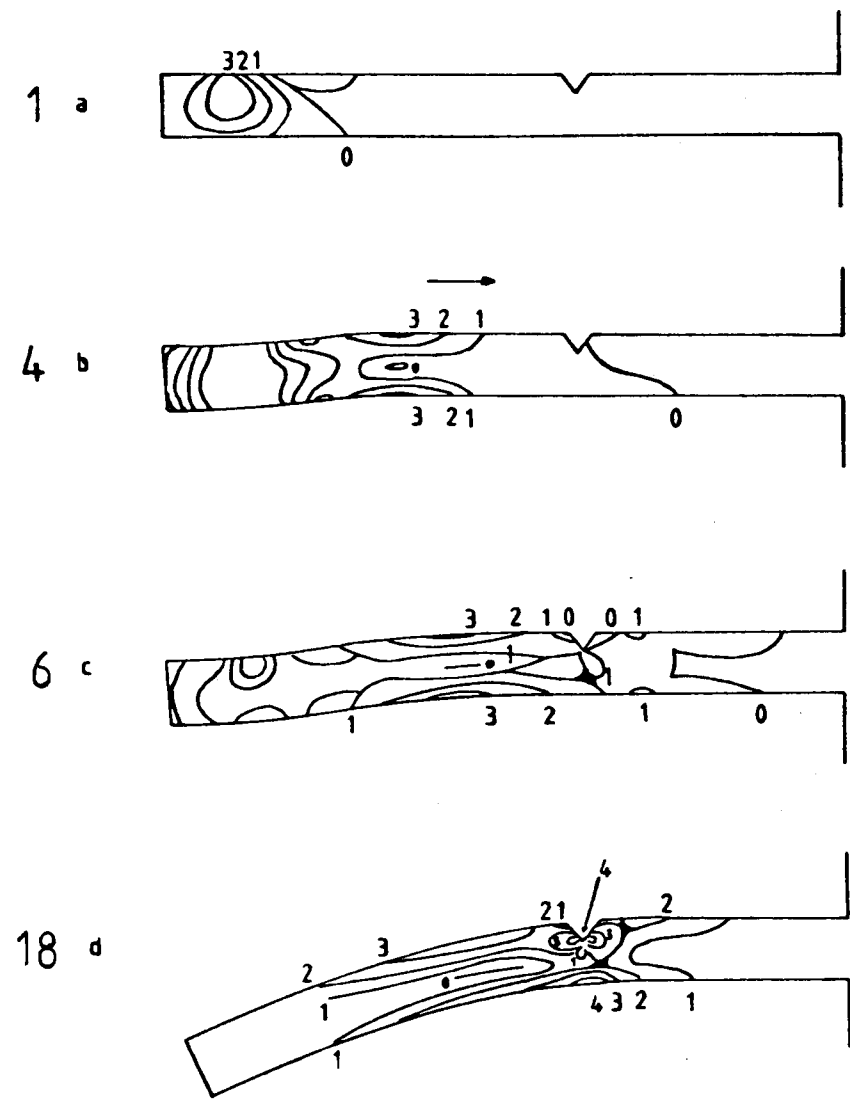


Fig. 4.9 Fringe pattern development for the Slender Cantilever.

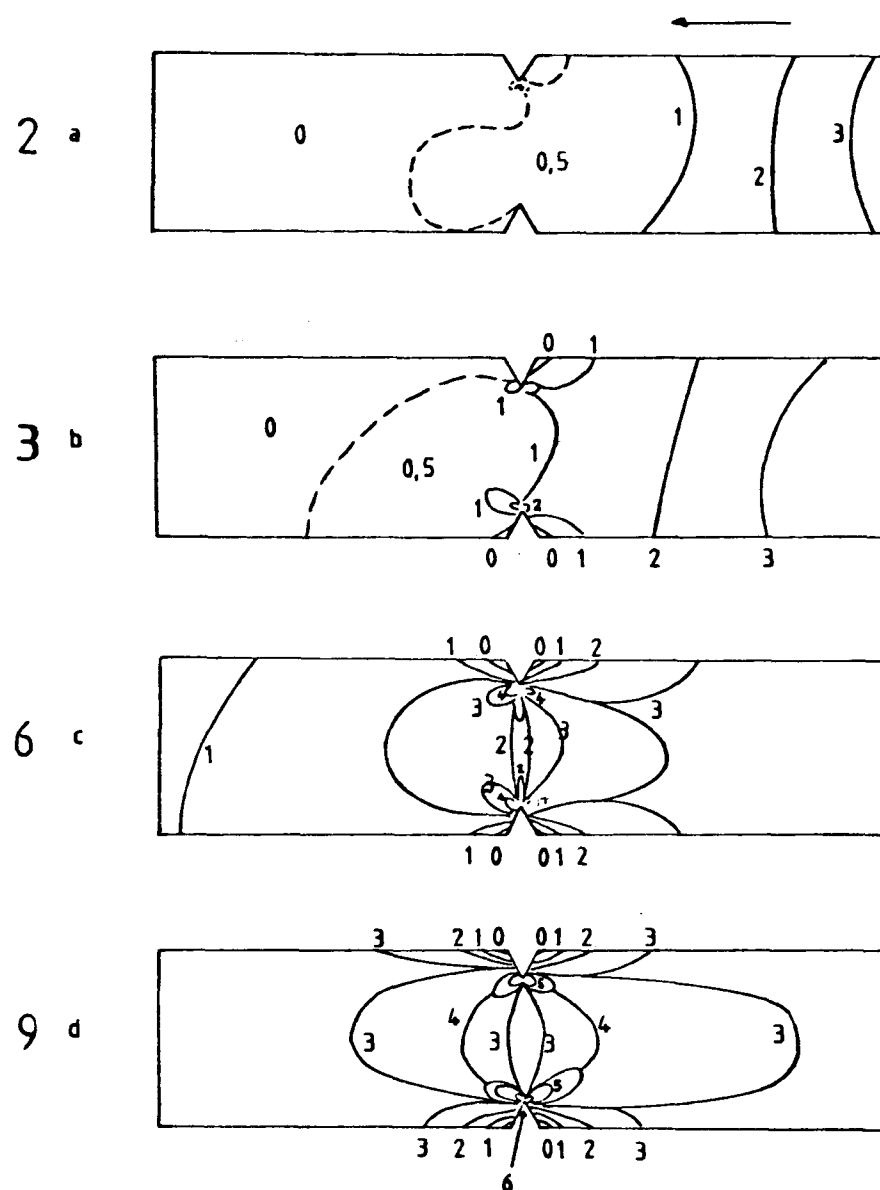


Fig. 4.10 Fringe pattern development for the Double Notched Bar.

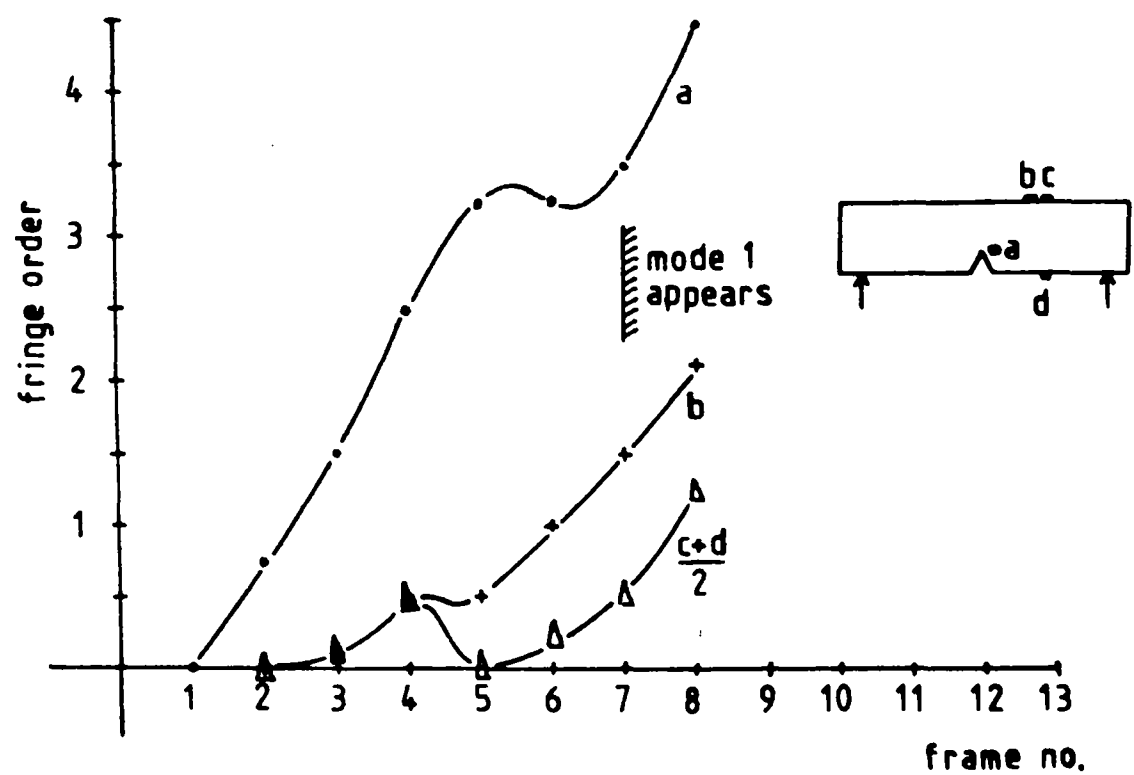


Fig. 4.11 Fringe order v. frame number for the Charpy specimen.

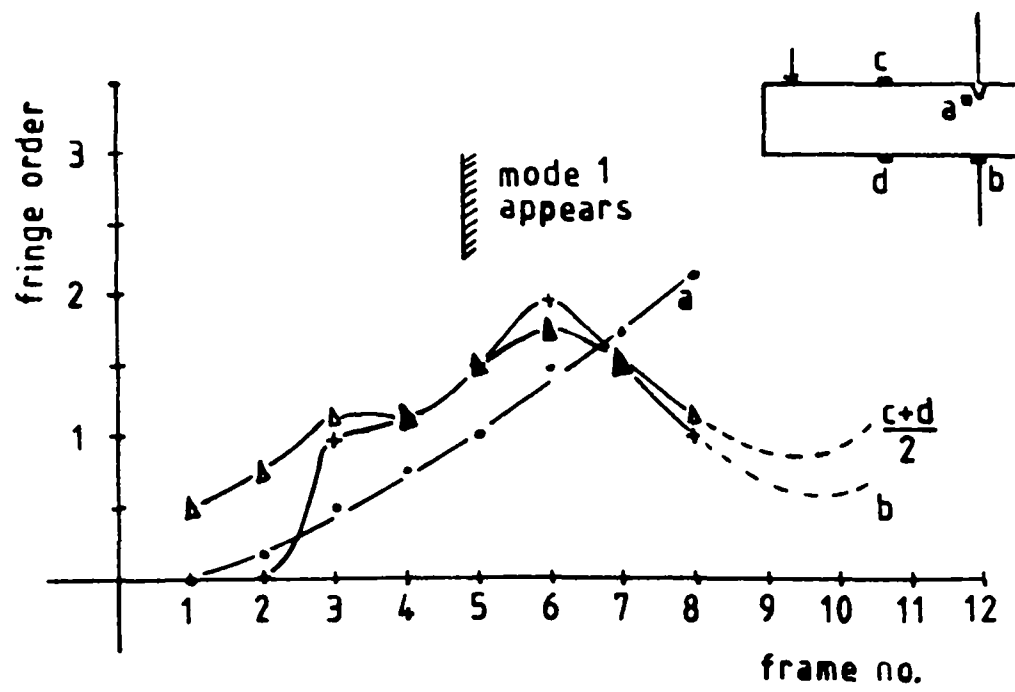


Fig. 4.12 Fringe order v. frame number for the Izod specimen.

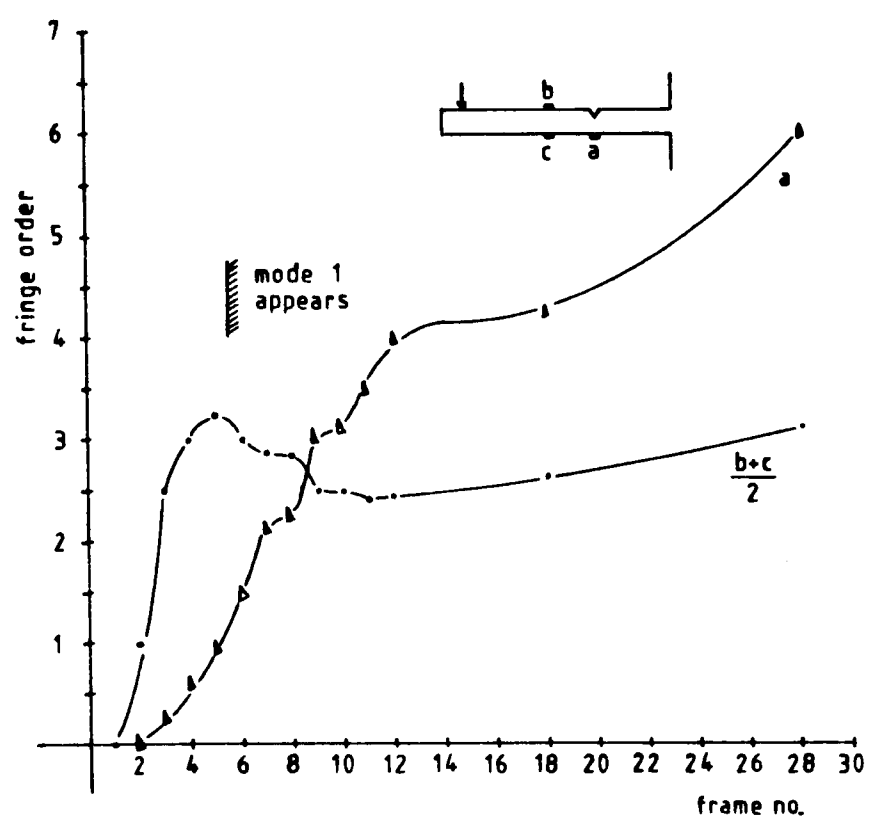


Fig. 4.13 Fringe order v. frame number for the Slender Cantilever.

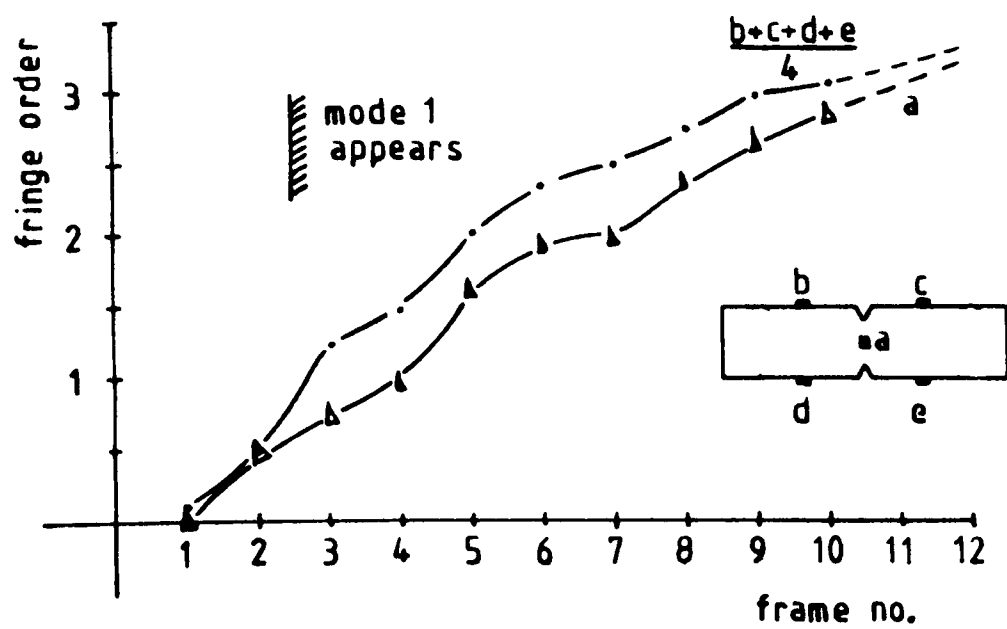


Fig. 4.14 Fringe order v. frame number for the Double Notched Bar.

CHAPTER V

THE DYNAMIC COMPLIANCE OF THE DOUBLE NOTCHED BAR AND CHARPY GEOMETRIES

5.1 THE DOUBLE NOTCHED BAR - A DYNAMIC PHOTOELASTIC ANALYSIS

5.1.1 Introduction

To summarize from the previous Chapter. In order to obtain a quantitative measure of the linear elastic dynamic response of the DNB an epoxy model material needs to be used necessitating the development of a $10^5 - 10^6$ fps photographic capability (see Section 2.4.2.2). As such a facility did not exist, a single spark system was developed using relatively cheap and simple components.

5.1.2 The Single Spark Photoelastic Camera

5.1.2.1 Optics Including the Light Source

The use of sparks in high speed photography is well described by Früngel^[1], and a typical custom made system was developed by Taylor^[2]. In the event a well proven system manufactured by Pulse Photonics Ltd., Southampton, was used. The specification of the spark source is given in Table 5.1. The source is most widely used on firing ranges. The source was selected for its high energy - a dark field polariscope absorbs 80% of incident light and filtering was also necessary. A dilatational wave in Araldite travelling at 2000 ms^{-1} (see Table 2.1) will traverse 0.22 mm in 100 ns. Assuming isochromatic fringes travel at a similar speed then blurring of the image should not occur. Note that the relation between stress pulse and fringes is complex^[3]. For example, in an infinite medium, in which both dilatational and distortional waves occur, the epoxy will only

become birefringent under shear. In the case of a finite body complex reflections take place at the boundaries.

The spark is fired in an atmosphere of Argon giving a more stable and brighter spark. A spherical mirror is positioned to the rear of the spark to reflect more light. The wide spectrum of the spark source means that it can be readily used for photoelasticity. In fact the light is filtered using a combination of a yellow (Kodak No. 12), green (Kodak No. 58) and peacock blue filters. The film used in the camera was 5" x 4" Kodak Tri X Ortho film which is especially sensitive to green light.

Fig. 5.1 gives a schematic of the lens polariscope, set up to give isochromatic fringes with the spark source and Fig. 5.2 gives a general view of the apparatus. The photoelastic bench was aligned using a mercury arc lamp. The polariscope was set up to give a dark field.

5.1.2.2 Electronic Instrumentation

The aim of the instrumentation was to trigger the light source at a specific time, to microsecond accuracy, and to monitor the stress wave behaviour of the impactor and specimen. The light source was triggered using a semiconductor strain gauge (gauge length = 0.5 mm, gauge factor = 100) mounted on the impacting weight (see Fig. 5.3). The signal from the 5 V DC bridge circuit was amplified and fed into a comparator circuit (see Fig. 6.10). The net result was a 14 V step pulse which triggered a delay generator with a variable delay between 0 and 9999 μ s. This delay generator then fired the spark by a short produced from an electro optical isolator. Such a triggering mechanism is reproducible and fast (approximately 6 μ s without any delay).

The stress levels in the impactor and supports were measured using metal foil strain gauges (gauge length = 6 mm, gauge factor

= 2.13). The dynamic response of such strain gauges has been discussed by Dally and Riley^[8]. A dilatational wave in Araldite traverses the gauge length in 3 μ s, and for a stress pulse of rise time 40 μ s obtained from the tests it is felt that the response of the gauges is adequate.

The response of the differential amplifiers and transient recorders is discussed in Section 6.2.3. The amplified signal was stored digitally in the recorders and the signal could either be displayed on a slave oscilloscope or plotted on an X-Y plotter.

Thus to obtain a record of dynamic fringe patterns the impact was repeated (see below) and the light fired at different times for each reproducible impact. Hence a series of pictures was built up.

5.1.2.3 The Loading Frame, Tup and Impacting Weight

The requirement was to build a rigid loading frame that could impact the specimen, in the required manner, reproducibly and controllably. The frame was made out of heavy duty square section crossbraced to ensure maximum rigidity (see Fig. 5.5). The pendulum suspension point was made reasonably massive and ball bearing races were used to reduce friction to a minimum. The impacting weight was made out of a single steel block (see Fig. 5.6) and the suspension tube was made as light as possible. Thus the weight was concentrated at the impact point and the pendulum was reasonably rigid. The weight can be dropped from various heights using an electromagnet. The whole frame stands on four feet that can be adjusted for height, level and stability.

In the case of the DNB it was decided to have a single impacting weight. This decision was taken after trying a two weight impact method (see Fig. 5.7). As we are interested in stress wave loading, as opposed to inertial loading, the formation of the stress pulse is critical. A two weight system may twist causing an out of phase

between the impact of one side and the other. If inertial loading is being considered the two weight system can be used. Fig. 5.8 shows the single impacting weight system. The weight, on the end of the pendulum, penetrates the tup and impacts face A. The initial position of the pendulum was 30° above the horizontal. Note that the use of a single weight system precludes the use of a drop weight. The tup is supported on a massive 'V' block.

The impact of the weight on face A displaces that section to the right and also gives rise to compressive waves that reflect from the free end as tensile waves. Thus the resultant tensile pulse is made up of two components (see Fig. 5.9), and the addition of these components depends on the dimension X. This dimension was selected to give the fastest initial rise time. In fact this initial pulse is part of a more complex stress wave response (see Fig. 5.17). The response consists of a high frequency oscillation, resulting from the stress wave going back and forth in the Araldite specimen, superimposed on a low frequency inertial oscillation, resulting from the tup being connected to the Araldite 'spring'. We are interested in the stress wave response of the specimen and so only the first 40 μ s will be considered in the subsequent analysis. Once the pulse is formed it travels down the steel tup, being modified by the changes of section, and passes into the specimen. The specimen was joined to the steel tup using Araldite glue.

5.1.2.4 The DNB Specimen

The specimen was milled from a 5 mm thick precast sheet of Araldite CT200 purchased from Sharples Ltd. The material properties are given in Table 5.2. E (static) was measured in an Instron tensile testing machine using a clip gauge for strain measurements, E (dynamic) was measured from strain gauge readings, and f_0 was derived from a calibration test using a tensile specimen^[5]. It can be seen that the

rate sensitivity of the material is negligible for the strain rates of the DNB tests.

The geometry of the DNB is given in Fig. 5.10, the overall dimensions being restricted by the diameter of the polariscope. The depth of the notch was selected to give proportions typical of fracture toughness tests. The shape of the notch is discussed in detail in Section 5.1.4.3, suffice it to say that the notch had to be blunt enough to ensure that the specimen did not fracture on impact - we are interested in the growth of stress around a stationary notch. After some trials a 0.2 mm wide saw cut was found to be blunt enough to avoid fracture whilst ensuring acceptable mode I loops (see below).

5.1.3 Conduct of a Test

The instrumentation was primed, the still camera shutter opened (in a darkened laboratory), the weight dropped, the light fired and then the camera shutter closed. The resultant traces are shown in Fig. 5.4. Trace A, from a standard photocell with no voltage or current applied, shows the light pulse. Trace B shows the pulse given by the delay generator hence these two traces show whether the light is firing in a stable manner. Trace D gives the stress pulse as measured by the strain gauge on the specimen, and trace C locates the light with respect to this pulse. Traces E and F are used to derive the timing of the firing of the spark. The initial step in trace F can be regarded as the time at which the weight impacts.

Note that the pulse shape (trace D) must be reproducible and can also be affected in later stages by diffraction from the notch tips. It was found that reproducibility was within 5% and that the effect of the diffracted pulse was negligible.

5.1.4 The Response of the DNB Specimen to the Incident Pulse

The global response was similar to that found in the 'Photoflex' tests, i.e. mode I 'butterfly' loops grow as the incident stress increases. The aim now is to extract stress intensity factors from these loops and hence to derive the variation of K_I with time due to an incident stress pulse $\sigma(t)$. This is now possible due to the low strain rate sensitivity of Araldite and the high quality of fringe patterns (see Fig. 5.29).

5.1.4.1 Data Reduction Method: Static Linear Shape Method

The data reduction methods for dynamic photoelastic experiments have been shown to be many and varied^[6]. It has been shown that for a dynamic stress field in the vicinity of a stationary notch, the singular stress field can be characterized by a form similar to the static case (see Section 2.3.2.1). Thus the static linear slope method developed by Morton and Ruiz^[7] should be applicable to this specific dynamic case.

For pure mode I, the Westergaard solution, neglecting far field stress, gives: (see Fig. 5.11)

$$\sigma_{xx} = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) + \dots$$

$$\sigma_{yy} = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) + \dots$$

$$\sigma_{zz} = \nu(\sigma_{xx} + \sigma_{yy}) \quad (\text{Plane strain}) \quad (5.1)$$

$$\tau_{zz} = 0 = \tau_{zy}$$

$$\tau_{xy} = \frac{K_I(t)}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \cos \frac{\theta}{2} \cos \frac{3\theta}{2} + \dots$$

Photoelasticity enables the determination of $(\sigma_1 - \sigma_2)$ loci, hence:

$$\begin{aligned}\sigma_1 - \sigma_2 &= \sqrt{(\sigma_{xx} - \sigma_{yy})^2 + 4\tau_{xy}^2} \\ &= \frac{1}{\sqrt{2\pi r}} K_I(t) \sin \theta\end{aligned}$$

For $\theta = 90^\circ$, i.e. along a line normal to the crack (see Fig. 5.11):

$$\sigma_1 - \sigma_2 \Big|_{\theta = 90^\circ} = \frac{1}{\sqrt{2\pi r}} K_I(t) \quad (5.2)$$

But in photoelasticity theory, birefringence is related to the maximum shear stress:

$$\text{i.e. } (\sigma_1 - \sigma_2) = \frac{Nf_\sigma}{h} \quad (5.3)$$

A dimensionless stress intensity factor $k_I(t)$ can be defined by:

$$K_I(t) = k_I(t) \sigma_{\text{nom}} \sqrt{\pi a} \quad (5.4)$$

Combining (5.2, 5.3 and 5.4) gives:

$$k_I(t) \sigma_{\text{nom}} \sqrt{\pi a} = \sqrt{2\pi r} \frac{Nf_\sigma}{h}$$

hence,

$$k_I(t) = \frac{f_\sigma}{\sigma_{\text{nom}} h} \cdot \frac{N}{\sqrt{\frac{a}{2r}}}$$

Given that for a specific fringe pattern k_I , σ_{nom} , f_σ , a and h are constant then a plot of N v. $\sqrt{\frac{a}{2r}}$ will give a line with gradient

$$\frac{k_I(t) h \sigma_{\text{nom}}}{f_\sigma}.$$

Hence $k_I(t)$ can be derived. The assumptions in this analysis are discussed below.

The value of the nominal stress is derived from the foil strain gauge positioned a quarter the way along the specimen (see Fig. 5.10).

The strain gauge bridge is shown in Fig. 5.12. It is a constant voltage circuit with parallel balancing to compensate for differences in R_1 , R_2 , R_3 and R_4 . Typically R_5 and R_6 are approximately 25 k Ω whereas the other resistances, including the gauge, are approximately 120 Ω . Hence the usual circuit analysis gives:

$$\Delta V \approx \pm \frac{V}{4} \frac{\Delta R_1}{R_1}$$

where $\frac{\Delta R_1}{R_1} = K\epsilon$, i.e. ΔR_1 is the change in resistance due to the applied strain. Combining these two identities gives:

$$\Delta V = \frac{VK\epsilon}{4}.$$

For a uniaxial stress field $\sigma = E\epsilon$ hence:

$$\sigma = \frac{4\Delta VE}{VK}.$$

This analysis has been checked against the calibration for the metal bars described in Chapter VI.

The assumptions made in the above data reduction technique are as follows:

1. The nominal stress is taken as the stress at the strain gauge extrapolated to the notched section, assuming a given pulse velocity, and neglecting the reduction in ligament size due to the notches.
2. The strain gauge measures the dynamic pulse accurately.
3. A uniaxial stress field exists in the specimen.
4. Linearity exists between ΔV and ΔR_1 , as given.
5. Global values of f_σ and E can be taken.
6. A plane strain, two dimensional, state dominates.
7. The validity of the static linear slope method.
8. Accuracy of measurements from the photographs.
9. The notch gives an acceptable singularity dominated field.

5.1.4.2 Discussion of Assumptions in the Data Reduction

An unnotched bar was strain gauged at quarter points and half point. The pulses measured from these three gauges were very similar showing that within one pass of the specimen dispersion was minimal and that material damping was minimal. This experiment was also used to derive a dynamic value of E assuming the pulse was dominated by P waves. The expected response of the foil strain gauges has already been discussed (see Section 5.1.2.1). The orientation of the strain gauge was in the direction of pulse propagation and so the strain gauge reflects accurately the incident loading in the specimen. The selection of strain gauges and circuitry is well discussed by Taylor^[2]. Given the use of foil gauges it was felt that a simple constant voltage circuit would yield an accurate strain value. This circuit has been used extensively in other dynamic tests^[9]. The measurement of f_0 and E has been described in Section 5.1.2.4. Of course in any dynamic stress distribution about a crack there is a large variation of strain rate. It has been shown that the effect of strain rate on E is negligible. It is assumed that this is valid for f_0 also.

In the DNB the 2D stress state is independent of thickness whereas strains are affected by thickness, i.e. whether a plane stress or plane strain regime dominates. Birefringence is a result of shear strain, hence it should be expected that there is a variation of fringe order through the thickness. Fringes obtained in these experiments are an average through the thickness. A state of plane strain is assumed to exist in the Araldite - this is borne out by the fracture surfaces after failure. The validity of the linear slope method is discussed in Ref. 7 and further discussion will be given in Section 5.2.5. The monitoring of the performance of the apparatus was given in Fig. 5.4. It was found that the photographs

were reproducible for a given delay and the maximum error is $\pm 1 \mu s$.

5.1.4.3 The Effect of the Shape of the Notch

There has been extensive discussion of the effect of notch shape in static stress intensity factor measurement in photoelastic tests [10,11]. Comparisons are made between natural, i.e. fatigue, cracks and notches of various shapes and acuity. In our case we were limited by the fact that fracture was not to occur after each impact. It took 24 hours to glue the specimen in place, hence fracturing on each impact would have been unrealistic. Various methods of making the notch were tried. A thin single blade cutter gave a skew notch. A thicker, multibladed, cutter was tried next, with the finishing from a blunt razor blade. The notch gave residual stresses and fractured after one impact. This method gave a 45° notch angle and tip radius of 0.015 mm. Previous work completed at Oxford^[12] included crack pre-cast into Araldite models: these cracks were 0.2 mm wide and gave acceptable results. Similar notches were produced by a 0.2 mm thick hack-saw blade. The detailed stress distribution in the vicinity of a 0.2 mm rectangular notch is discussed in Section 5.2.5.

5.1.4.4 Analysis and Discussion of Results

Fig. 5.13 gives the incident stress pulse and the times at which the light fired. From the resultant photographs the growth of fringes can be studied (see Fig. 5.14). The stress pulse travels down the specimen in the form of increasing and decreasing fringe values (see Fig. 5.14a). The stress ramp interacts with the notches to produce mode I loops in the vicinity of the notches (see Fig. 5.14b, c). The stress peak has to attain the notched section before stress waves are shed downstream (Fig. 5.14d). Stress levels start to decay as the pulse passes through the notched section (Fig. 5.14e). No reflections have occurred from the far end in this time. The mode I loops at the notch tip can be photographically enlarged and the stress

intensity factor measured (see Section 5.1.4.1). Fig. 5.15 shows the experimental scatter from two typical tests. It is expected that the range of validity of the linear slope method is $1 < \sqrt{\frac{a}{2r}} < 3.5$ (see Section 5.2.5). The fringe positions were measured by eye and it can be seen that data points yield a straight line. Table 5.3 gives the number of valid, and invalid, data points used to derive values of $K_I(t)$ and $k_I(t)$. Also given are the maximum and minimum limits of $\sqrt{\frac{a}{2r}}$. It can be seen that data points can be taken in the far field outside the limit of $1 < \sqrt{\frac{a}{2r}}$. When $\sqrt{\frac{a}{2r}} = 1$, $r = \frac{a}{2}$ i.e. 2.5 mm. When $\sqrt{\frac{a}{2r}} = 2.5$, $r = 0.5$ mm. The nature and the effect of the far and near fields are discussed in Section 5.2.5. The possible error caused by displacing the origin of measurements by 0.1 mm is 15%. In the case of the DNB results the origin can be located within 0.05 mm. A systematic variation occurs between upstream and downstream fringes - this variation occurred for all data (see Table 5.3). Fig. 5.16 gives the variation in $K_I(t)$ and $k_I(t)$. Static values are also displayed. Note that k_I (static) is proportional to the nominal stress.

$K_I(t)$ increases in a non-oscillatory manner, slightly lagging the applied stress. $k_I(t)$ reduces to a static value at the peak load - this would seem to be entirely fortuitous. Large errors occur in $k_I(t)$ at low loads. The difference between upstream and downstream values would seem to occur due to the finite tip radius of the notch - the singularity is weaker than for a sharp crack. Tests carried out with sharp notches, in which the specimen broke after each impact, gave the same stress intensity factors from upstream and downstream loops. With a multiple spark camera a sharp notch could be analysed more readily. Note that a caustic forms at the tip of a sharp notch at high stresses and so the notch tip cannot be located accurately.

5.1.4.5 Conclusions

An elastodynamic *calibration* has been derived for a Double Notched Bar subjected to a ramp loading of 36 μ s duration. It has been shown that the response is non-oscillatory conforming closely to the incident stress pulse. The accuracy of photoelastic data is dependent on many assumptions but high confidence can be placed on the raw data and data reduction.

In dynamic fracture toughness tests the usual material under study is a metal and a dynamic compliance is required for this. In static photoelasticity the scaling from model to prototype stresses is straightforward^[13,14]. In the case of stress wave loading scaling is more difficult given the complex dependence of different stress waves on different material properties. In the case of inertial loading scaling is less problematic^[15]. Thus for stress wave loading a numerical analysis is required to scale from photoelastic results to metal specimens. In other words the numerical program is proved for the photoelastic data and then run with the relevant metal material properties. The result is an elastodynamic *calibration* for a metal specimen subjected to a ramp loading.

5.2 THE DOUBLE NOTCHED BAR - A NUMERICAL ANALYSIS

5.2.1. Introduction

The various numerical programming techniques available for the analysis of elastodynamic crack problems were described in Section 2.4.3. The simplest approach for stationary cracks is the finite element approach using a singularity element adapted from static fracture mechanics^[16]. For this study such a dynamic finite element program was not available at Oxford. The boundary element approach was tried^[17] but solutions for transient problems were found to be intractable. Thus an existing finite difference package, written by J. Goicolea of

King's College, London, was implemented on the departmental VAX 11/780. This program has been well proven for a variety of problems^[18], its main use being in the modelling of plastic deformation. A major disadvantage with finite difference programs is that the steep stress gradients in the vicinity of crack tips are modelled with difficulty (see Section 2.4.3). It was felt that a 0.2 mm wide notch should be able to be analysed without an extrapolation scheme in the first instance. The Explicit Finite Difference program is described in detail in Appendix A and the results only will be described in this Chapter. Initially the program was tested for various simple dynamic conditions before the full problem was run.

5.2.2 Results for an Unnotched Bar Subjected to a Triangular Pulse

The pulse incident on the notched section (see Fig. 5.13) was idealized as a triangular pulse. The discretization of the bar was coarse (see Fig. 5.18), being made up of plane strain triangles. It was assumed that there was no damping, that E was rate independent and Araldite material properties were taken. The latter were slightly different from the photoelastic tests - compare Table 5.2 with Table 5.4. Stress, force and displacement histories were monitored for the elements and nodes shown in Fig. 5.18. Fig. 5.19 compares the imposed pulse with the pulse as it is numerically modelled travelling down the bar. It can be seen that an accuracy of at least 10% is achieved.

5.2.3 Results for a Notched Bar Subjected to a Step Pulse

The occurrence of the notches complicates the mesh - the number of cells increase from 80, in the previous problem, to 1396. Fig. 5.20 shows the discretization. The discretization was achieved by an automatic data generation facility. The notches were modelled as rectangles 5 mm deep and 0.2 mm wide. The computing time greatly

increased due to the greater number of cells and the smaller times required for each cycle due to the Courant condition (see Appendix A). Stresses σ_{yy} were tabulated for cells along $\theta = 0^\circ$ (see Fig. 5.11) and plotted against $\sqrt{\frac{1}{r}}$. In a singularly dominated field, from equations (5.1):

$$\sigma_{yy} = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2} \right) + \dots$$

for $\theta = 0^\circ$:

$$\sigma_{yy} = \frac{K_I(t)}{\sqrt{2\pi r}}$$

Thus the gradient of the straight line corresponds to

$$\frac{K_I(t)}{\sqrt{2\pi}}$$

Figure 5.21 gives the response to the step input in terms of $k_I(t)$ where $K_I(t) = k_I(t) \sigma_{nom} \sqrt{\pi a}$. The nominal stress is calculated from the response of the unnotched bar to the same input pulse. From Rooke and Cartwright^[19] the static $k_I = 1.13$ for a distant applied stress - as the point of application of the load approaches the notched section so k_I rises slightly. $k_I(t)$ rises before the step - this is due to disparity between idealized pulse and the pulse derived numerically (see for example, Fig. 5.19). $k_I(t)$ then oscillates, due to negligible damping, increasing towards k_I (static). The computing time ran out before the oscillations decayed to zero. The accuracy of measurement of the gradient

$$\frac{K_I(t)}{\sqrt{2\pi}}$$

will be discussed in the next Section, but it can be seen that the notch gives slightly low values of stress intensity factor using the above data reduction technique.

5.2.4 Results for a Notched Bar Subjected to a Triangular Pulse

The discretization was as that described in Section 5.2.3 and the imposed pulse as that described in Section 5.2.2. Zero damping was assumed, the geometry is as shown in Fig. 5.10 and the material properties as given in Table 5.4. Values of nominal stress were taken from data for the unnotched bar at the time corresponding to the elapsed period after the leading edge of the triangular pulse arrived at the notched section. $K_I(t)$ and $k_I(t)$ values were derived from the σ_{yy} values as described in the previous Section. Fig. 5.22 shows the results compared with those from the photoelastic tests. Values of $K_I(t)$ derived from the program are lower due to low values of $k_I(t)$. The oscillations in $k_I(t)$ continue throughout the response. Why is there this difference? To answer this question the stress distribution in the vicinity of the notch will be discussed in detail.

5.2.5 Discussion of the Stress Distribution in the Vicinity of the Notch

Fig. 5.23 gives the elements in the vicinity of the notch. The questions to ask are what is the extent of the singularity dominated field and how accurate is the derivation of the gradient of the $\sigma_{yy} \propto \sqrt{\frac{a}{2r}}$ plot. Fig. 5.24 gives the $\sigma_{yy} \propto \sqrt{\frac{a}{2r}}$ plot for $t = 30 \mu s$.

Morton and Ruiz^[7] show that non-linearities occur in the linear slope method due to near field, i.e. finite notch tip radius, and far field, i.e. loss of dominance of the singularity effects. Phang^[12] has studied this problem for static photoelasticity and he deduced the limits defined in Table 5.5. These limits are compared to Smith et al.^[10] and the photoelastic results derived in the thesis. Smith et al.'s results can be relaxed somewhat with only small loss of accuracy hence Phang's limits will be

taken. It can be seen that the range of validity of the numerical data is covered by the graph of Fig. 5.24 and that the resultant slope represents most accurately the data points. It could be argued that the data points need to be modified, for large $\sqrt{\frac{a}{2r}}$, to take into account the finite notch tip radius. From Morton and Ruiz^[7] the stress components become, for Mode I only:

$$\begin{aligned}\sigma_{xx} &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 - \sin \frac{\theta}{2} \sin \frac{3\theta}{2}\right) - \frac{K_I}{\sqrt{2\pi r}} \frac{\rho'}{2r} \cos \frac{3\theta}{2} \\ \sigma_{yy} &= \frac{K_I}{\sqrt{2\pi r}} \cos \frac{\theta}{2} \left(1 + \sin \frac{\theta}{2} \sin \frac{3\theta}{2}\right) + \frac{K_I}{\sqrt{2\pi r}} \frac{\rho'}{2r} \cos \frac{3\theta}{2} \\ \tau_{xy} &= \frac{K_I}{\sqrt{2\pi r}} \sin \frac{\theta}{2} \cos \frac{\theta}{2} \cos \frac{3\theta}{2} - \frac{K_I}{\sqrt{2\pi r}} \frac{\rho'}{2r} \sin \frac{3\theta}{2}\end{aligned}$$

in which the origin is shifted from the tip to $(-\frac{\rho'}{2}, 0)$. For $\theta = 0^\circ$:

$$\sigma_{yy} = \frac{K_I}{\sqrt{2\pi r}} \left[1 + \frac{\rho'}{2r}\right]$$

For $\sqrt{\frac{a}{2r}} = 2.50$, $\frac{\rho'}{2r} = 0.125$ hence a 13% error should be expected in the gradient. For $\sqrt{\frac{a}{2r}} = 1.00$, $\frac{\rho'}{2r} = 0.02$, i.e. 2%. The far field non-linearity is more difficult to assess, depending as it does on boundaries and far field loading. When r is at the axis of symmetry of the DNB specimen $\sqrt{\frac{a}{2r}} = 0.5$. After this point the other singularity starts to dominate. Various values of $\sqrt{\frac{a}{2r}}$ have been superimposed on Fig. 5.23.

From the discretization it would seem that the limits of $1.12 < \sqrt{\frac{a}{2r}} < 2.24$ would be most accurate, increasing the slope slightly in Fig. 5.24 and hence increasing k_I .

In a singularity dominated field other stresses should show singular behaviour: for example, for σ_{xx} at $\theta = 90^\circ$. From Equations (5.1):

$$\sigma_{xx} = \frac{K_I(t)}{\sqrt{2\pi r}} \cos \frac{\pi}{4} \left(1 - \sin \frac{\pi}{4} \cdot \sin \frac{3\pi}{4}\right)$$

$$= \frac{K_I(t)}{\sqrt{2\pi r}} \frac{1}{2\sqrt{2}}$$

The variation of σ_{xx} at $\theta = 90^\circ$ with respect to $\sqrt{\frac{a}{2r}}$ is given in Fig. 5.25. It can be seen that the scatter is even worse than for σ_{yy} at $\theta = 0^\circ$, and for the line given in Fig. 5.25 the corresponding value of $k_I(t)$ is 1.23 giving a $K_I(t)$ of 381 kPam^{-1/2}. This value is an improvement on σ_{yy} values at $\theta = 0^\circ$. Note that there is little difference between upstream and downstream values in this case.

5.2.6 Conclusions

The derivation of $K_I(t)$ and $k_I(t)$ from numerical data is difficult. Linear slope plots give large scatter and the resultant slopes are open to interpretation. It is felt that although a singularity element is not so realistic the resultant data would be more easy to interpret. The present program could be continued to be used for elastodynamic analyses but results for the metal specimen may be inaccurate. Of course the finite difference program would be useful in modelling any inelastic behaviour but the current aim is to derive an elastodynamic calibration.

Results from EFD corroborate data from the photoelastic analysis viz the non-oscillatory response of the Double Notched Bar to an imposed ramp loading. Thus in developing a dynamic fracture toughness test based on the DNB geometry a ramp stress wave loading should be sought. Given this loading the stress intensity factor at the notch tip should increase linearly until fracture. Before expending any further effort on developing a dynamic calibration, e.g. running EFD with metal material properties or developing a finite element package, it was felt that the behaviour of a metal

specimen to the above loading conditions should be developed. In other words is an imposed stress wave ramp loading feasible and is the resulting fracture dominated by brittle fracture? These aspects are discussed in Chapter VI.

5.3 THE CHARPY GEOMETRY - A DYNAMIC PHOTOELASTIC ANALYSIS

5.3.1 Introduction

It was concluded in Chapter IV that the two geometries of the DNB and the Charpy, or 3 point bend, specimen needed to be studied in further detail. The specification of the DNB test presents few difficulties - the test can be summarized by 'a stress pulse incident on a double notched section in a two dimensional bar'. In the case of the Charpy geometry this specification is more difficult. It was shown in Chapter III that the response of the specimen depends on the interaction between the impactor, the specimen and the supports.

Given these difficulties an experimental set up had to be selected. In the event the apparatus shown in Fig. 5.26 was designed and built. The loading frame and pendulum designed for the DNB tests was adapted. The pendulum could not impact the specimen directly because a minimum time delay of 10 μ s was required between impact and the light firing to account for the delay in the switching circuits. Hence an intermediate tup was used. The single shot spark technique and lens polariscope were again used to obtain dynamic isochromatic fringes in Araldite.

5.3.2 The Impactor, Specimen and Supports

A one-dimensional wave analysis of the weight/tup system, assuming no specimen, is given in Appendix B. The net effect is to drive the metal pointer into the Araldite specimen causing the specimen to react. The load trace is from a 6 mm gauge length foil

gauge mounted on the tup. The response of the gauge is faster than for a similar gauge on an Araldite specimen given that a P wave passes the gauge in 1 μ s. The rise time of the pulse, which is dependent on impact velocity and area, is relatively slow.

The specimen was milled from a 5 mm thick precast sheet of Araldite CT200 purchased from Sharples Ltd. The material properties are given in Table 5.2. The dimensions of the specimen are given in Fig. 5.26, the maximum dimension being limited by the diameter of the polariscope and the aspect ratio being the same as that for a typical Charpy V notch metal specimen. The notch was made using a 0.2 mm thick hack-saw blade, the depth being the same percentage of the overall depth as a Charpy V notch.

The impacting weight was modified by the addition of an extra 30 mm in order to ensure that a maximum stress level was obtained prior to unloading at A' (see Fig. 5.28). The support of the specimen was by means of radiused line contacts. The supports were integrated into a massive and rigid structure.

5.3.3 Conduct of the Tests

The conduct of the tests was similar to that for the DNB experiments. The tup was rested against the specimen prior to impact and it was found that the response of the specimen was reproducible. In these tests the specimen fractured for each impact but specimens could be replaced easily. The initial pendulum angle was 45° above the horizontal. The first series of tests concerned an unnotched beam.

5.3.4 Results for the Unnotched Beam

The growth of fringes is shown in Fig. 5.27 and the times for each photograph with respect to the stress wave in the tup, at the position of the strain gauge, is shown in Fig. 5.28. The motion of

the pointer, neglecting the focussing effect of the pointer, is given in Fig. B.3 . The pointer is driven into the specimen at a constant velocity. Initially the stress field is localized to the central portion of the beam (Fig. 5.27A). Boussinesq loops irradiate from the point of contact; these loops can be used to gain a measure of the applied force^[2]. Frocht^[20] has shown that, for the static case, for a particular fringe order the applied force is proportional to the diameter of the associated loop. Many factors affect the constant of proportionality. In the dynamic case loops may be distorted by high strain rate effects and stress wave reflections (see Fig. 5.27C).

In Fig. 5.27B flexural waves are transmitted to the supports; a slight asymmetry can be seen. The flexural waves are in the form of a zero fringe order in the centre of the beam. They are more pronounced in Fig. 5.27C. By this time tensile stresses have reflected from the opposite side of the specimen to the pointer. In Fig. 5.27D the supports start to take effect and the initial flexural deformation has dissipated. The Boussinesq loops are still increasing indicating an increasing load. There is a localized stress gradient in the central portion of the beam. The beam is now bending under its own inertia (Fig. 5.27E) and fringes corresponding to global bending begin to occur. A transition period occurs (Figs. 5.27E, F and G) as bending begins to predominate. Note the reduction of the contact force. By Fig. 5.27G the beam is bending under its own inertia and curvature continues to increase (Fig. 5.27H). In this analysis the maximum time after impact is $t = 177 \mu s$.

5.3.5 Results for the Notched Beam

A photograph of a typical fringe pattern is given in Fig. 5.29. Note the improvement of quality over the high speed ciné

results (see Fig. 4.6).

The first parameter to measure is the time of fracture. This was achieved by placing a 1 mm gauge length foil gauge (gauge factor = 2.15) adjacent to the notch tip (see Fig. 5.26). The resultant trace is given in Fig. 5.30. The time to fracture from weight impact was 359 μ s.

Fig. 5.31 gives the growth of fringes up to this time. The first frame is at $t = 98 \mu$ s from impact. Stress waves have been reflected a number of times in the central portion of the beam and flexural waves are travelling out towards the supports (Fig. 5.31A). Mode I butterfly loops are already well developed. These flexural waves dissipate and the supports start to take effect (Fig. 5.31B). The major stress gradients are concentrated in the central portion of the beam. There is now a transition from this localized behaviour to global bending behaviour (Fig. 5.31B, C). This transitional behaviour has already been discussed for the un-notched beam. Note that in the 'Photoflex' tests (see Chapter IV) this transitional behaviour was not so marked. The tup has now lost contact and the beam continues to bend under its own inertia (Fig. 5.31D). In Fig. 5.31E the tup has regained contact, modifying the stress field in the central portion of the beam. The beam starts to accelerate again and just prior to failure (Fig. 5.3F), the stress intensity factor at the notch tip is large giving rise to a caustic at the tip. This last fringe pattern should be compared to the static pattern (Fig. 5.31S). The static pattern is for a lower load but it can be seen that the components of each pattern, e.g. mode I loops and applied force, are similar.

5.3.5.1. The Measurement of Stress Intensity Factors

Given the well defined mode I loops at the notch tip throughout the response, stress intensity factors can be deduced using the

static linear slope method of Morton and Ruiz. The data reduction technique was exactly the same as that for the Double Notched Bar, i.e. fringe orders were measured along $\theta = 90^\circ$ and they were plotted against $\sqrt{\frac{a}{2r}}$ to give a straight line equivalent to

$$\frac{k_I(t)h\sigma_{nom}}{f_\sigma}.$$

The nominal stress was measured by placing a 1 mm gauge length foil strain gauge at the position of the notch tip in an unnotched bar. The resultant trace is given in Fig. 5.32. The resultant values of $K_I(t)$ and $k_I(t)$ are given in Fig. 5.33.

One problem with the interpretation of these graphs is that there are only a few photographs to analyse. Given the time consuming nature of the single shot technique only a restricted number of photographs were obtained. Oscillations do occur in the nominal stress. It is difficult to relate these oscillations to the applied load - the Boussinesq loops are not circular and so the method suggested in Section 5.3.4 cannot be used. Also the strain gauge on the tup does not give the applied load either (see Fig. 5.28).

The first oscillation corresponds to the transition from local shear to global loading, i.e. the transition from phase 1 to phase 2 (see Table 4.3). The second oscillation occurs when the flexural wave travels to the support and dissipates, i.e. transition from phase 2 to phase 3. The third oscillation occurs during the period of loss of contact of the tup and indicates that the bending of the beam under its own inertia is not a constant velocity process. The fourth oscillation is due to the regaining of the contact of the tup and the final oscillation, just prior to fracture, could correspond to a variation in the applied force - note the increase in Boussinesq loops from Fig. 5.31E to 5.31F.

The accuracy of the linear slope method for the DNB has already been discussed - see Sections 5.1.4.4 and 5.2.5. Table 5.6 gives the number of valid data points used in the derivation of $K_I(t)$ and $k_I(t)$ for the Charpy geometry. The scatter for these results was greater than for the DNB results (see Table 5.3 and Fig. 5.15). The notch tip was more difficult to locate due to the positioning of a crack initiation gauge in tests C, D and E and also data could only be derived for $\sqrt{\frac{a}{2r}} < 1.60$ for the same reason in tests C, D and E and due to blurring of high order fringes in test F. Thus values of $K_I(t)$ and $k_I(t)$ are more prone to error in this case. Note that low values of $\sqrt{\frac{a}{2r}}$ can be achieved indicating that the singularity domination extends further into the far field than in the case of the DNB. This is supported by the higher values of K_I .

The variation in $K_I(t)$ shows a definite sensitivity to the loss of contact, illustrating that the bouncing behaviour of the specimen does affect the growth of stress intensity. Note that the bouncing behaviour in this test is different to that described by Kalthoff^[21] and it will be shown that such behaviour differs from that of the Instrumented Charpy Tests described in Chapter VI. From Fig. 5.33 the value of K_{Id} is $2800 \text{ kPa}\sqrt{\text{m}}$. The variation of $k_I(t)$ is also given in Fig. 5.33 and is compared to the static value of k_I under 3 point bending, which is independent of load. Values of $k_I(t)$ for tests A, B and F, i.e. when a contact force occurs, are higher than for tests C, D and E, when the beam bends under its own inertia. After the tup regains contact $K_I(t)$ increases until failure, at which time $k_I(t)$ approximates to a static value. Thus although Servers' ' τ ' condition is violated, a quasistatic stress field may exist at fracture.

5.3.6. Conclusions on the Charpy Geometry

In Araldite, fracture occurs under brittle conditions, at a time of $\frac{t_f}{\tau} = 1.55$ for the given experimental conditions. The tup loses contact and then regains it prior to fracture, whereas the supports are always in contact. An elastodynamic *calibration*, i.e. plot of $K_I(t)$, has been obtained for the specific experimental conditions but it is felt that more data should be developed. Only six photographs were taken and more data points are required given the complex mechanical behaviour of the set up.

Such an elastodynamic *calibration* could then be used to prove the finite difference program, for the specific geometry and loading, and then the program run for an Instrumented Charpy Test. But would the loading conditions be the same? This problem has been discussed in Chapter III and Section 5.3.1.

If more fundamental data is to be gained from the Charpy test either the test should be specified exactly and a specific *calibration* sought or an alternative, more scientific, loading developed. In the former case the caustic method of Kalthoff or a numerical calibration could be used. The disadvantages of the Charpy pendulum or drop weight methods of loading are that for many dynamic cases the relationship between load measured from an instrumented tup and the actual applied load can be different. Also the nature of the impactor-specimen-support system greatly influences the dynamic response. To overcome these disadvantages an alternative method of loading needs to be devised (see Chapter VI).

5.4 CONCLUSIONS ON ANALYSIS AND DATA REDUCTION TECHNIQUES

It is felt that for the purposes of this current study the techniques used above are adequate. The single shot technique does introduce unwanted variations, though, and more extensive data is

required, especially for the Charpy test. A sharp notch with a multiple spark camera would yield a singularity dominated field of exact applied loading for which the accuracy of the linear slope method could be fully studied, e.g. accurate definition of the limits of validity. The linear slope method is applicable for the stationary crack but a more sophisticated data reduction method would be required as soon as the crack started to propagate (see Section 2.4.2.2). As far as the numerical analysis is concerned the finite difference method complicates a relatively simple problem by increasing the number of elements required and by giving rise to numerical problems in the vicinity of the notch. An extrapolation scheme, similar to HEMP^[22], could be incorporated into EFD and hence EFD could be used for both elastic and elasto-plastic analyses. On the other hand a special purpose finite element program could be developed for an elastodynamic analysis with a simple, static $\frac{1}{\sqrt{r}}$ singularity element.

5.5 REFERENCES, TABLES, FIGURES

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Duration of Flash	100 ns
Energy of Flash	2.5 J
Source Size	~10 mm
Jitter Time	500 ns

Table 5.1 Specification of the Pulse
 Photonics Spark Source

E (Static)	$3.2 \pm 0.1 \times 10^9 \text{ Pa}$
E (Dynamic)	$3.3 \pm 0.2 \times 10^9 \text{ Pa}$
Density	1120 kgm^{-3}
Fringe Value (Static)	$10.3 \pm 0.2 \times 10^3 \text{ Nm/fr}$
Poisson's Ratio	0.36

Table 5.2 Properties of Araldite CT-200 at Room Temperature

Test No.	Time From Weight Impact (μs)	σ_{nom} (MPa)	k_I	K_I (kPa $\sqrt{\text{m}}$)	Number of Valid Data Pts.	$\sqrt{\frac{a}{2r}}$	
						Min.	Max.
A U [†]	57.0	0.265					
D							
B U	107.0	1.237	1.50	233	3	1.21	2.28
D			1.79	278	3(+1)*	0.83	2.19
C U	112.0	1.826	1.46	334	3	1.27	2.64
D			1.59	364	3(+2)	0.58	2.50
D U	117.2	2.474	1.20	372	5	0.93	2.28
D			1.32	409	5	1.01	2.25
E U	121.4	2.886	1.14	412	5(+1)	0.80	2.32
D			1.31	474	5(+1)	0.87	2.19
F U	127.8	3.004	1.15	433	5(+1)	0.81	2.32
D			1.28	482	5(+)	0.80	2.11

[†]U $\equiv$ Upstream, D $\equiv$ Downstream, * Additional, non-valid data points in brackets.

Table 5.3 Photoelastic Results for DNB

E	$3.00 \times 10^9 \text{ Pa}$
ρ	1120 kgm^{-3}
ν	0.36

Table 5.4 Properties of Araldite used
 for Numerical Analysis

	$\overline{(\frac{a}{2r})}_{\text{min.}}$	$\overline{(\frac{a}{2r})}_{\text{max.}}$
Phang[12]	1.00	3.50
Smith et al.[10]	1.77	3.54
Photoelastic (DNB - Table 5.3)	0.80	2.30
Photoelastic (Charpy - Table 5.6)	0.60	1.60

Table 5.5 Limits for Data Reduction in the Linear Slope Method

Test No.	Time From Weight Impact (μs)	σ_{nom} (Mpa)	k_I	K_I (kPa $\sqrt{a}$)	Number of Valid Data Pts.	$\overline{\frac{a}{2r}}$ Min. Max.
A	98	3.47	0.94	408	8	0.80 2.27
B	147	5.12	1.07	685	9(+1)	0.53 1.58
C	198	7.43	0.89	827	3(+2)	0.58 1.42
D	252	9.57	0.80	957	3(+3)	0.52 1.39
E	302	13.37	0.84	1404	7(+1)	0.57 1.23
F	338	15.02	1.22	2291	6(+1)	0.56 1.28

Table 5.6 Photoelasticity Results for the Charpy Geometry

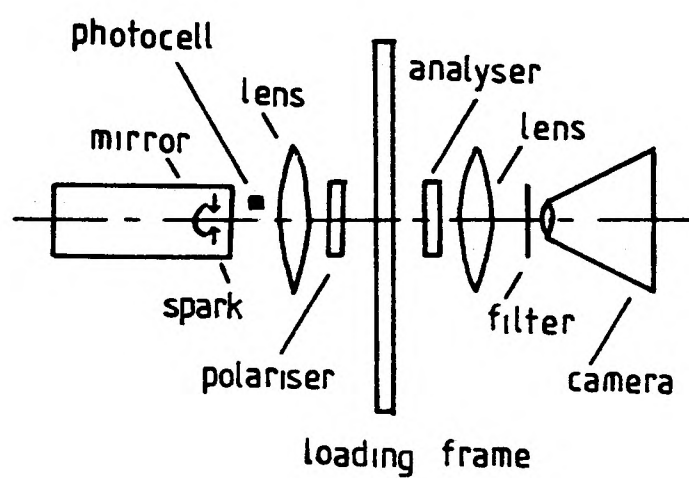


Fig. 5.1 Schematic of the single spark photoelastic bench.

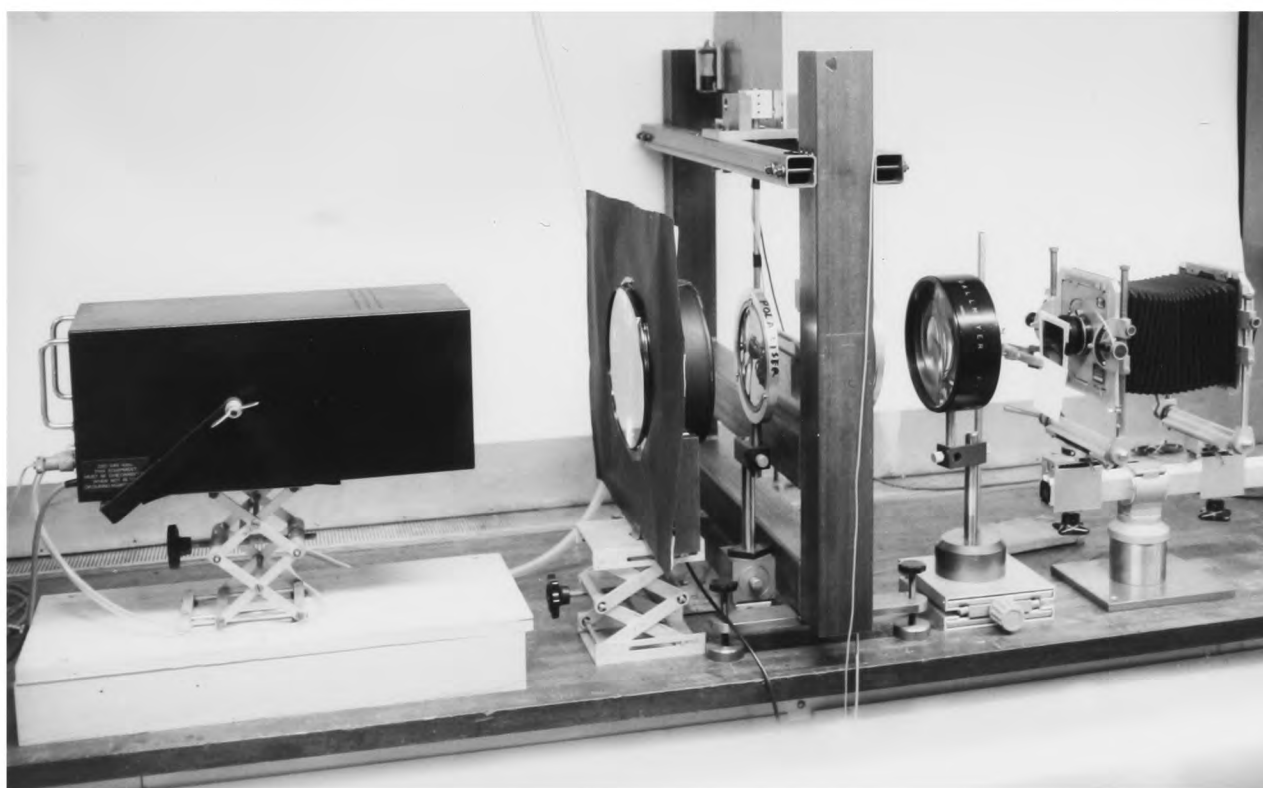


Fig. 5.2 General view of the single spark photoelastic bench.

Fig. 5.3 General arrangement of single spark instrumentation.

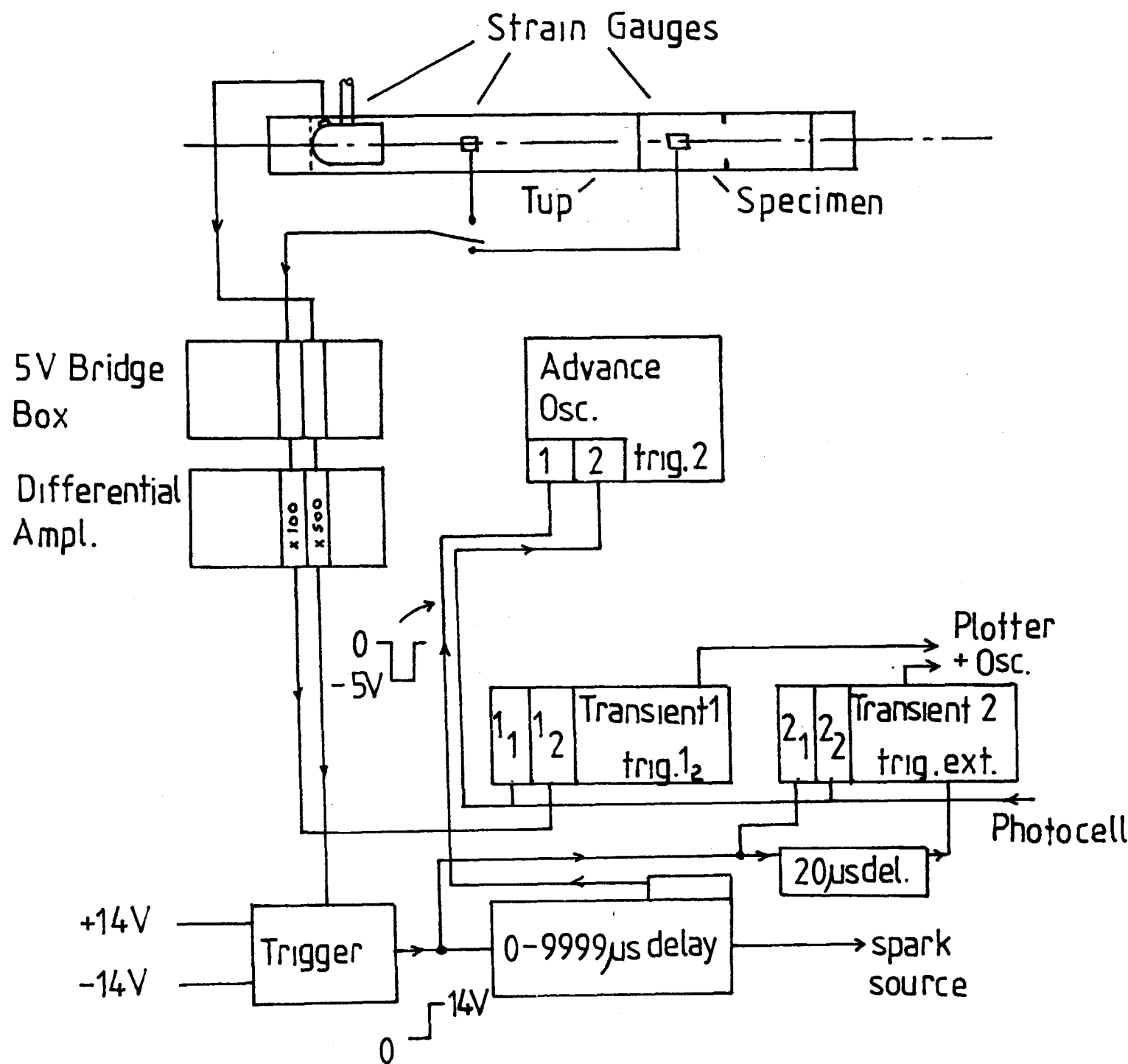
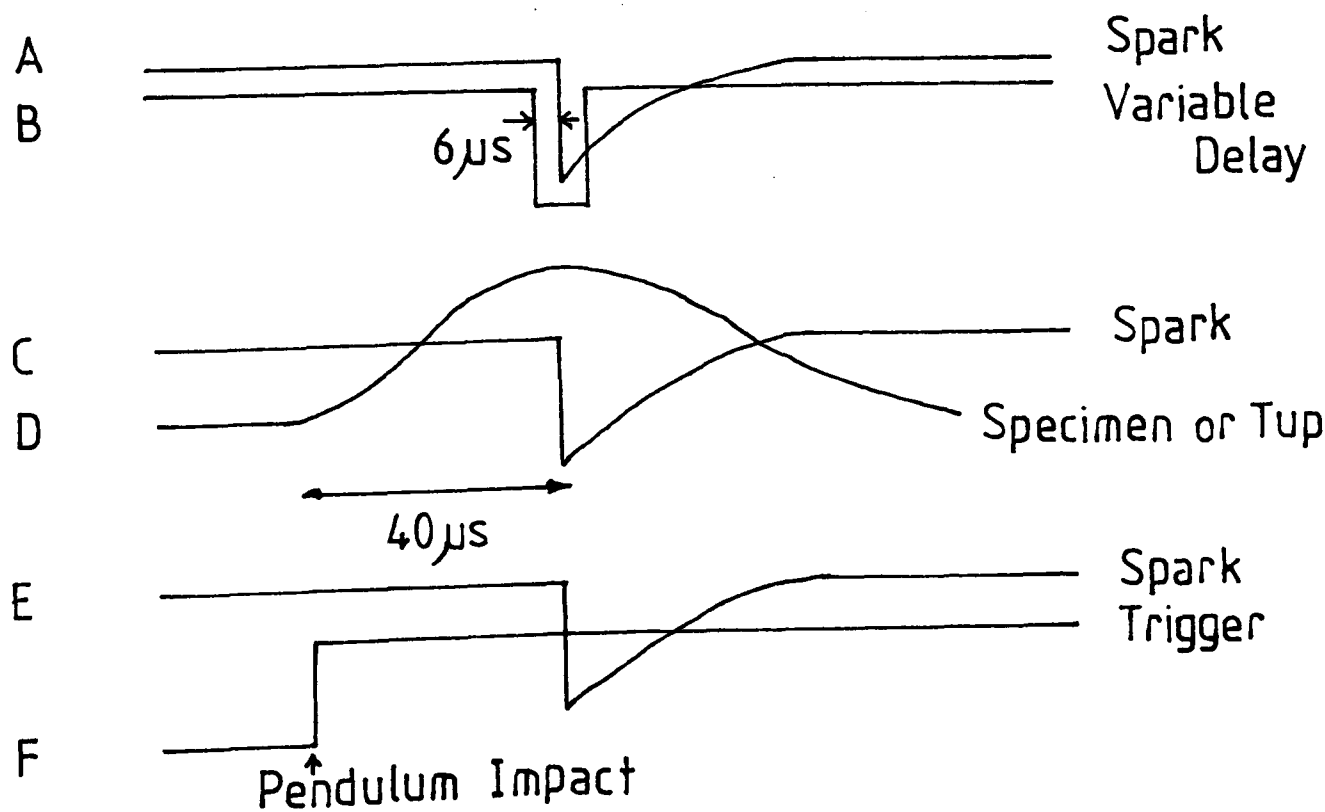


Fig. 5.4 Traces from a typical DNB impact test.



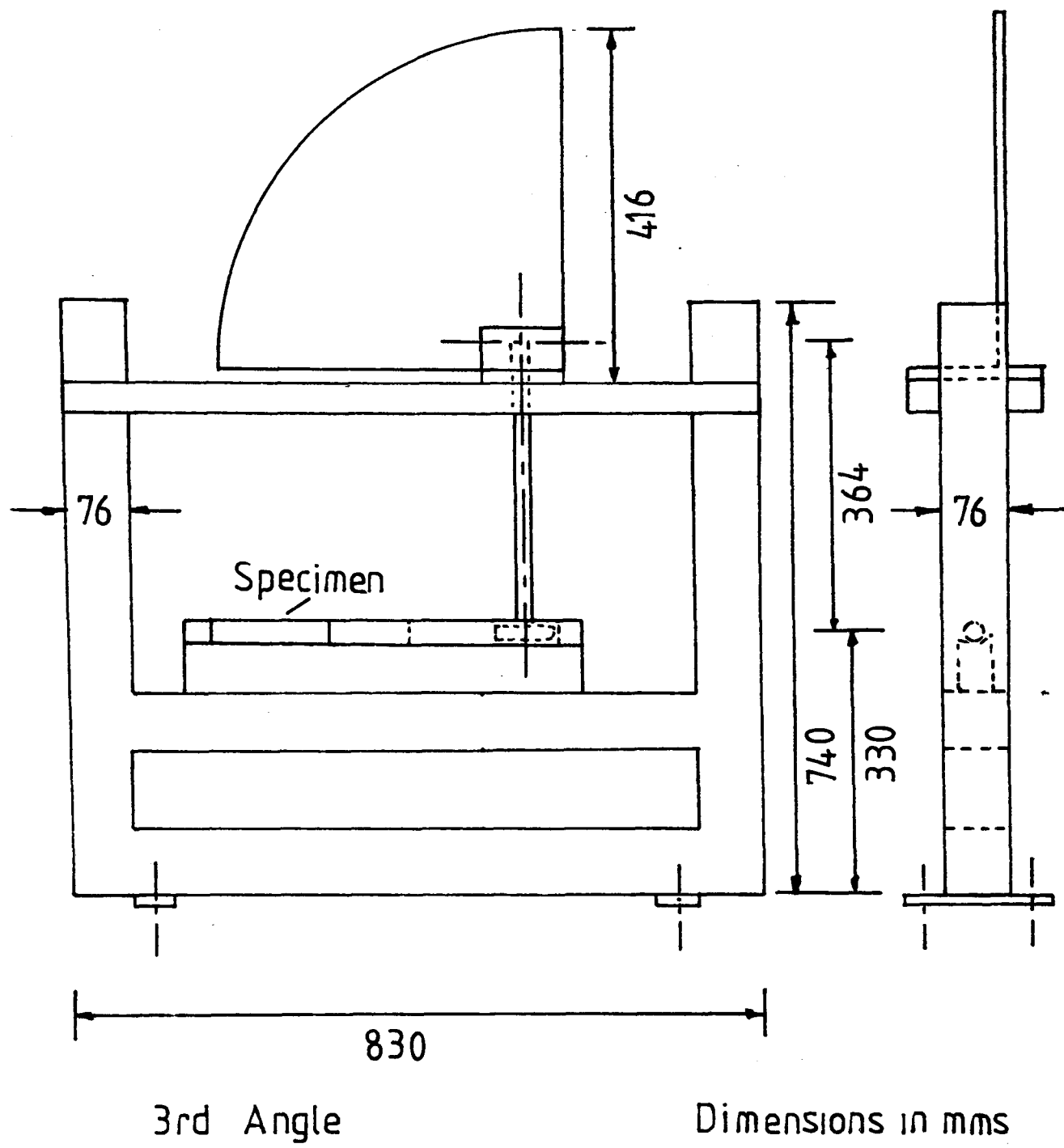


Fig. 5.5 General arrangement of DNB loading frame.

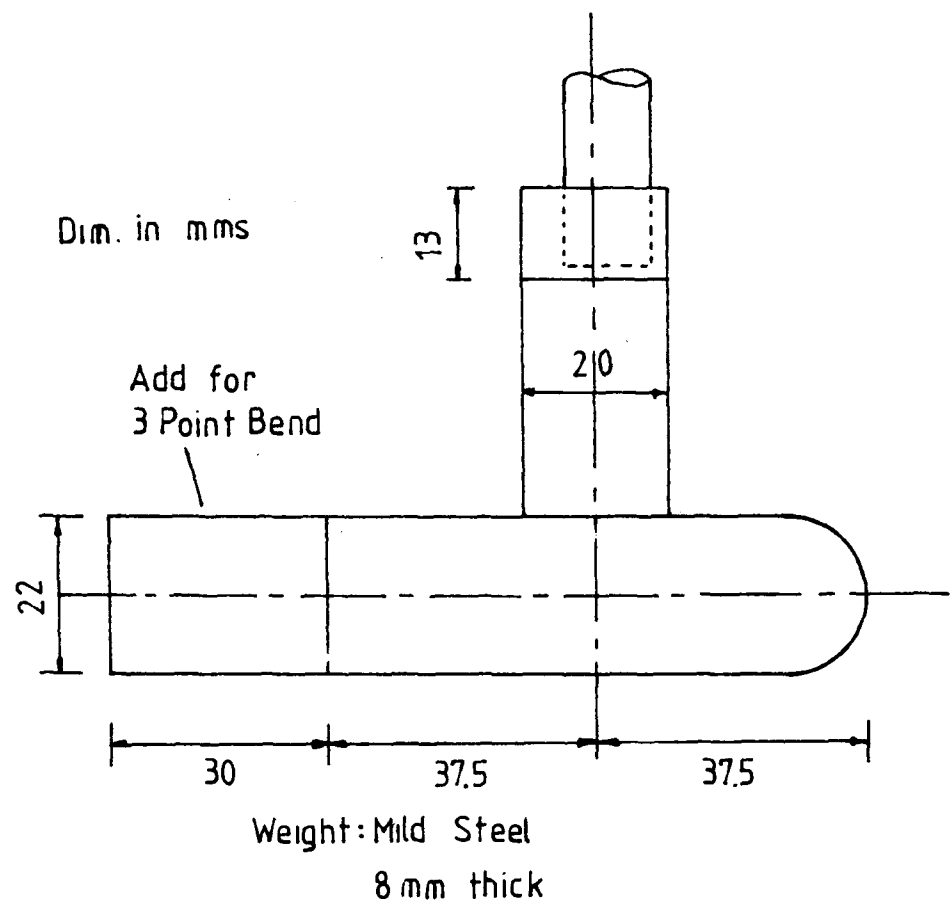


Fig. 5.6 Detail of impacting weight

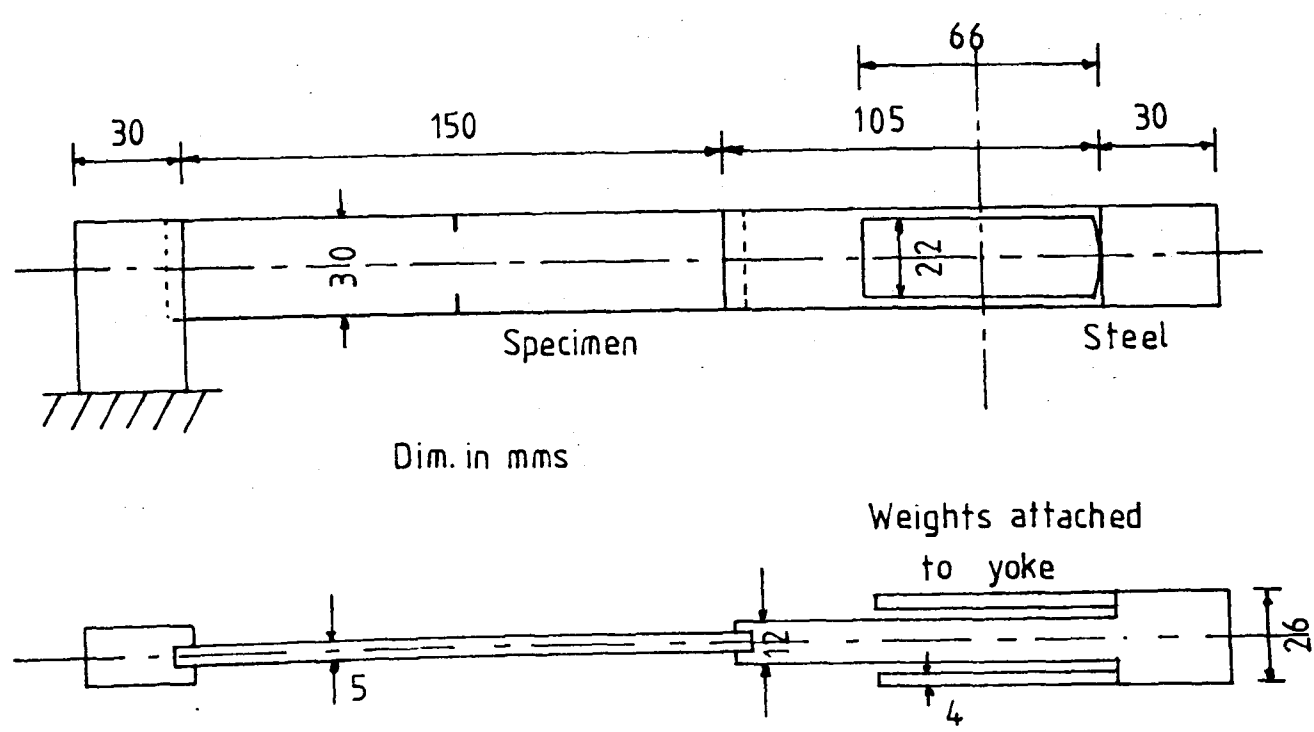


Fig. 5.7 Two weight impact method for DNB.

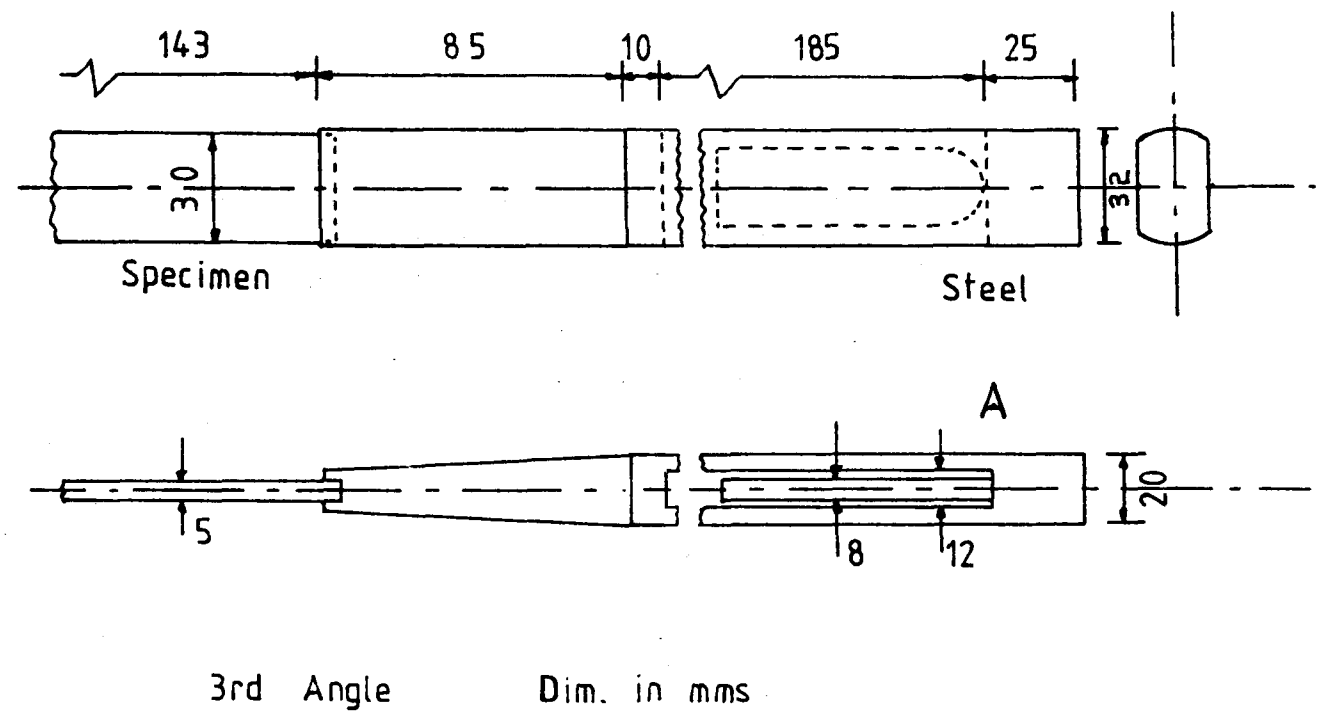


Fig. 5.8 Single weight impact method for DNB.

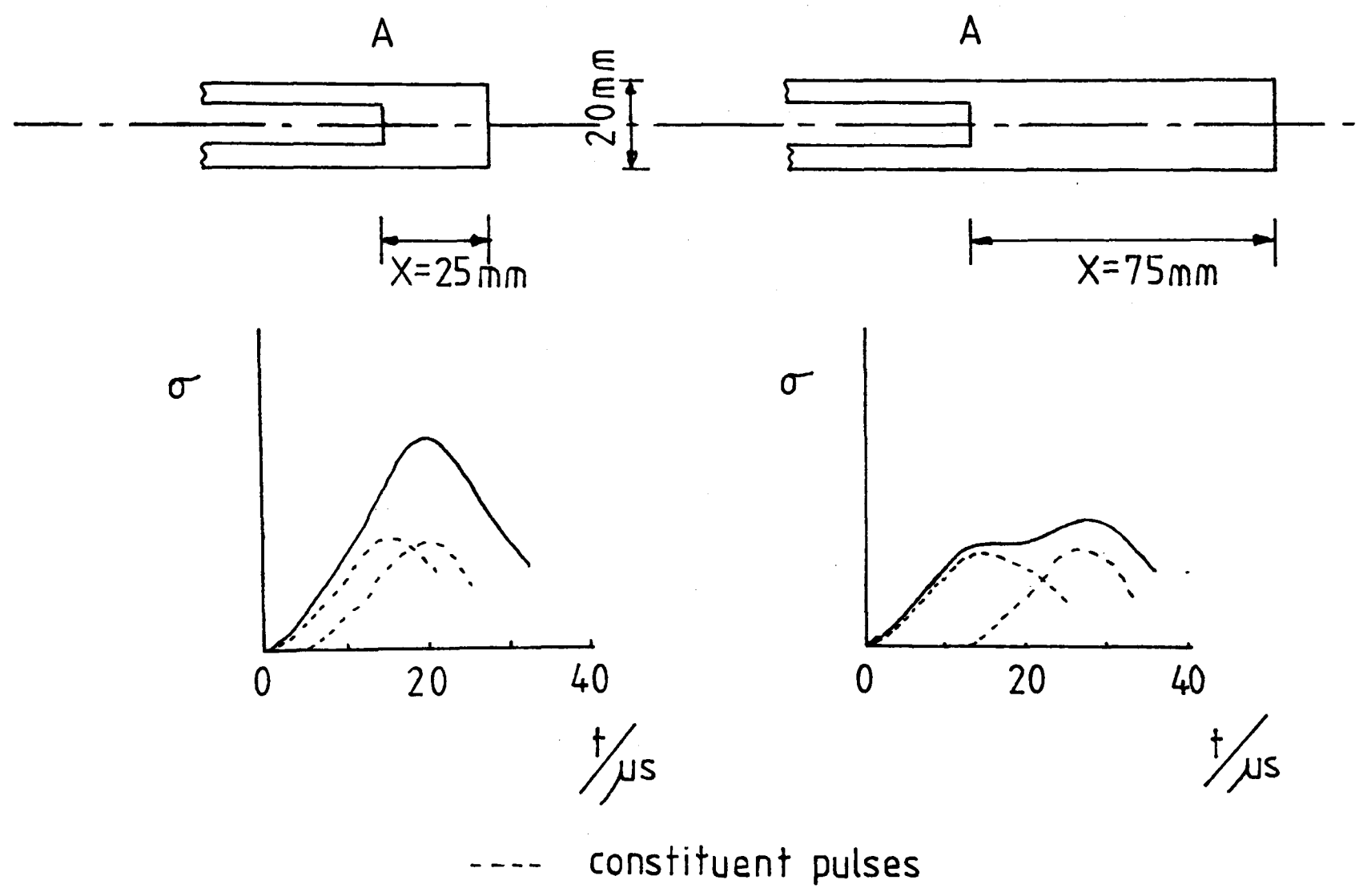


Fig. 5.9 Formation of the stress pulse for DNB.

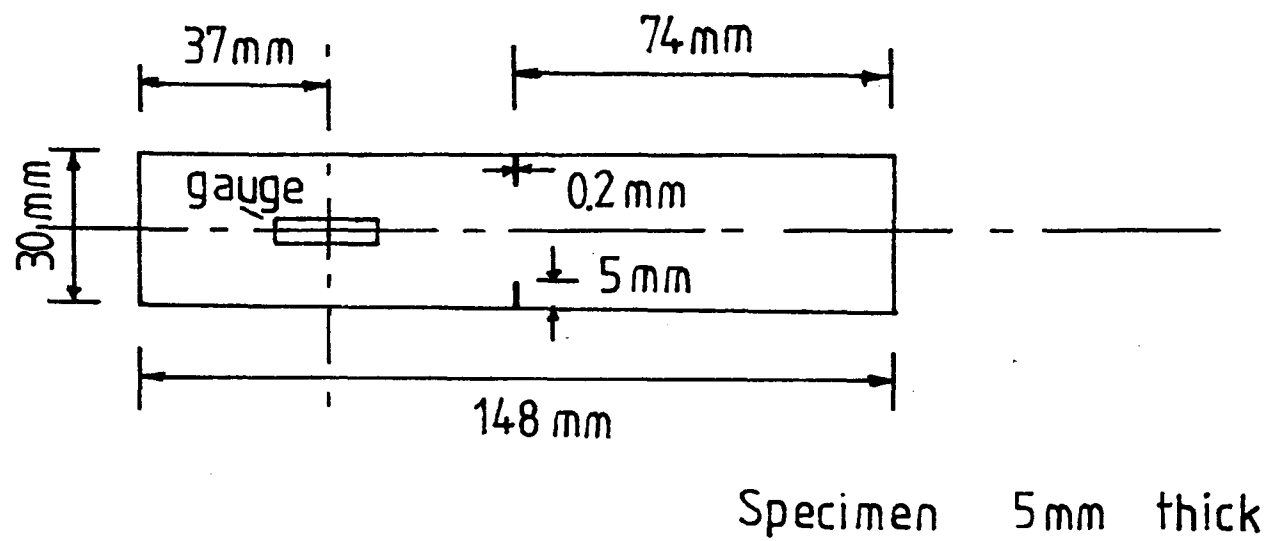


Fig. 5.10 DNB specimen geometry.

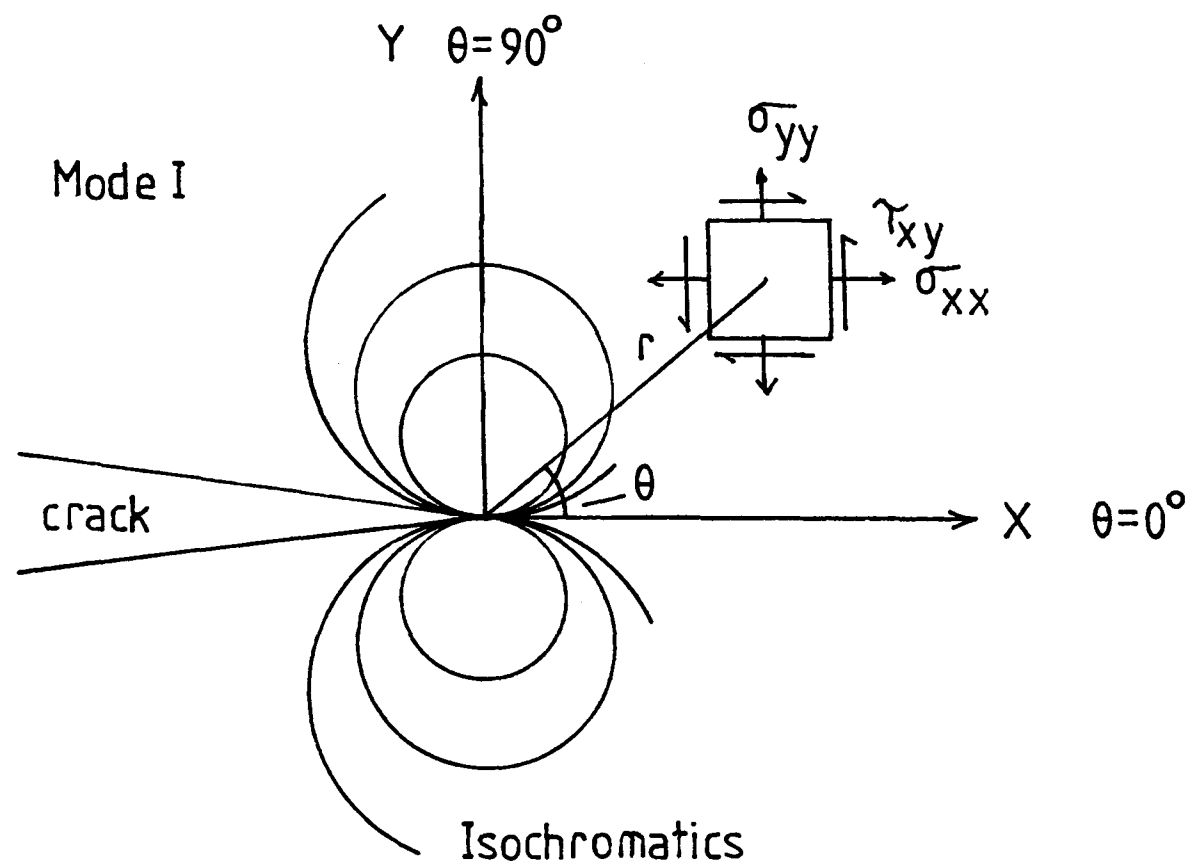


Fig. 5.11 Mode I isochromatic loops at a crack tip.

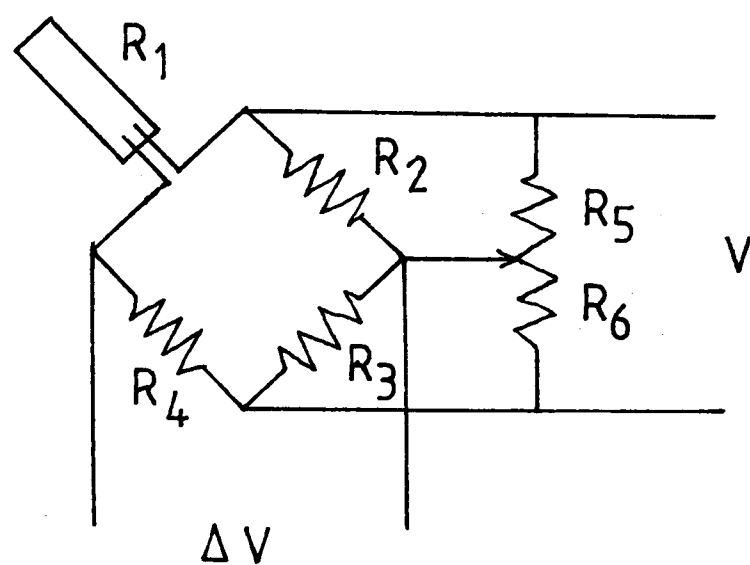


Fig. 5.12 Bridge circuit for nominal stress measurements.

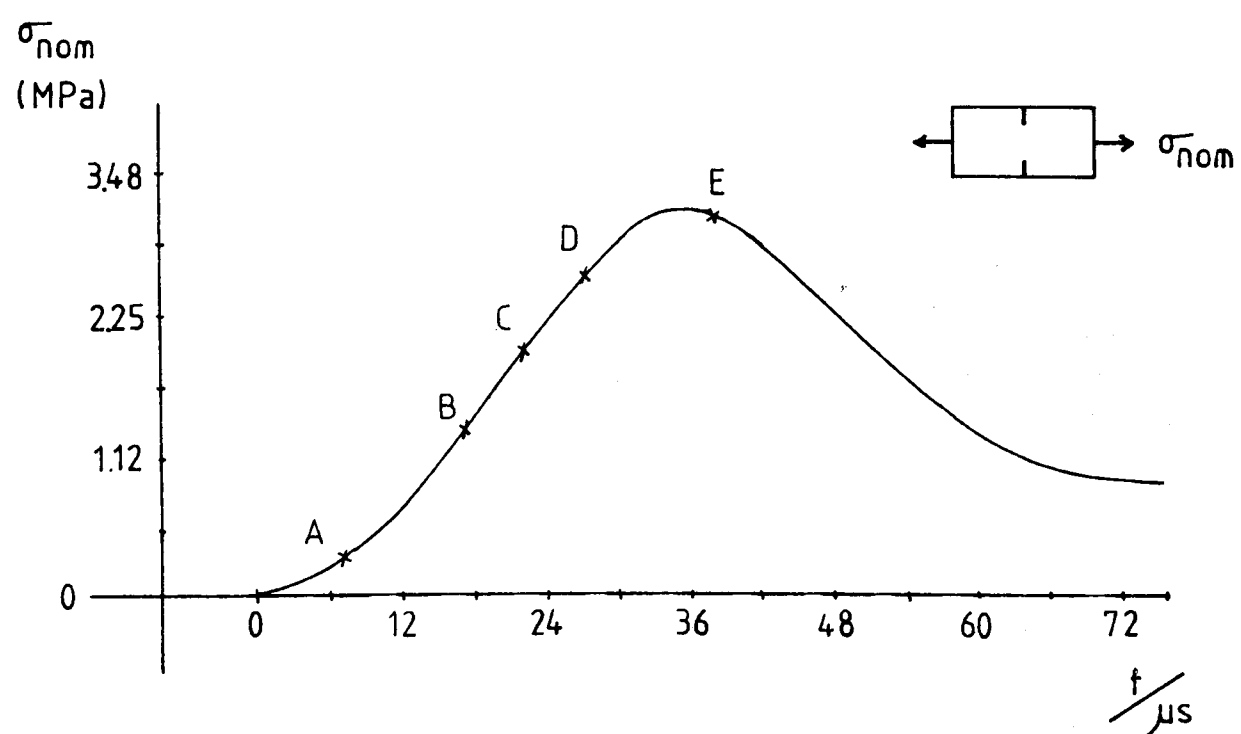


Fig. 5.13 Incident stress pulse for DNB geometry.

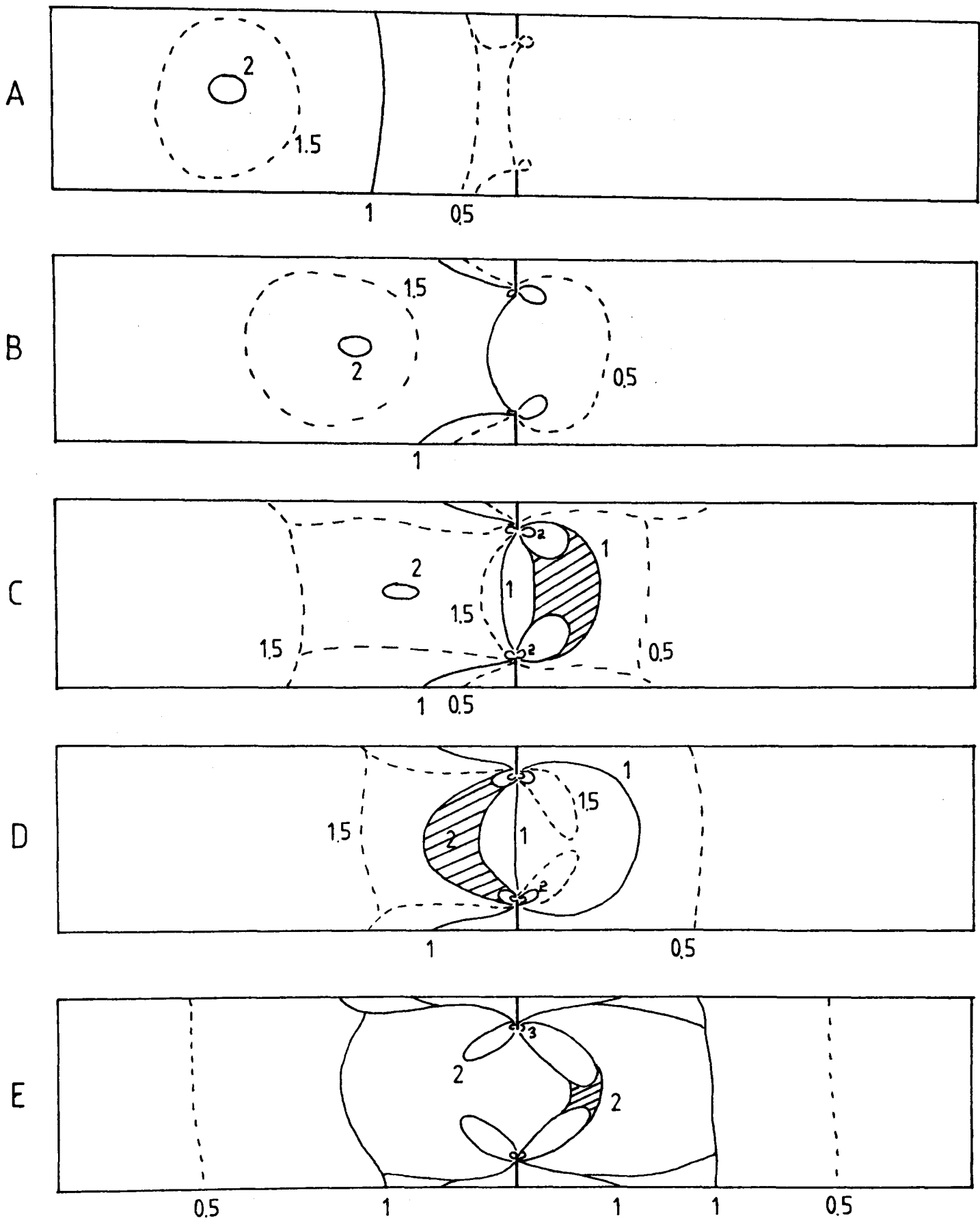


Fig. 5.14 Growth in isochromatic fringes for DNB test.

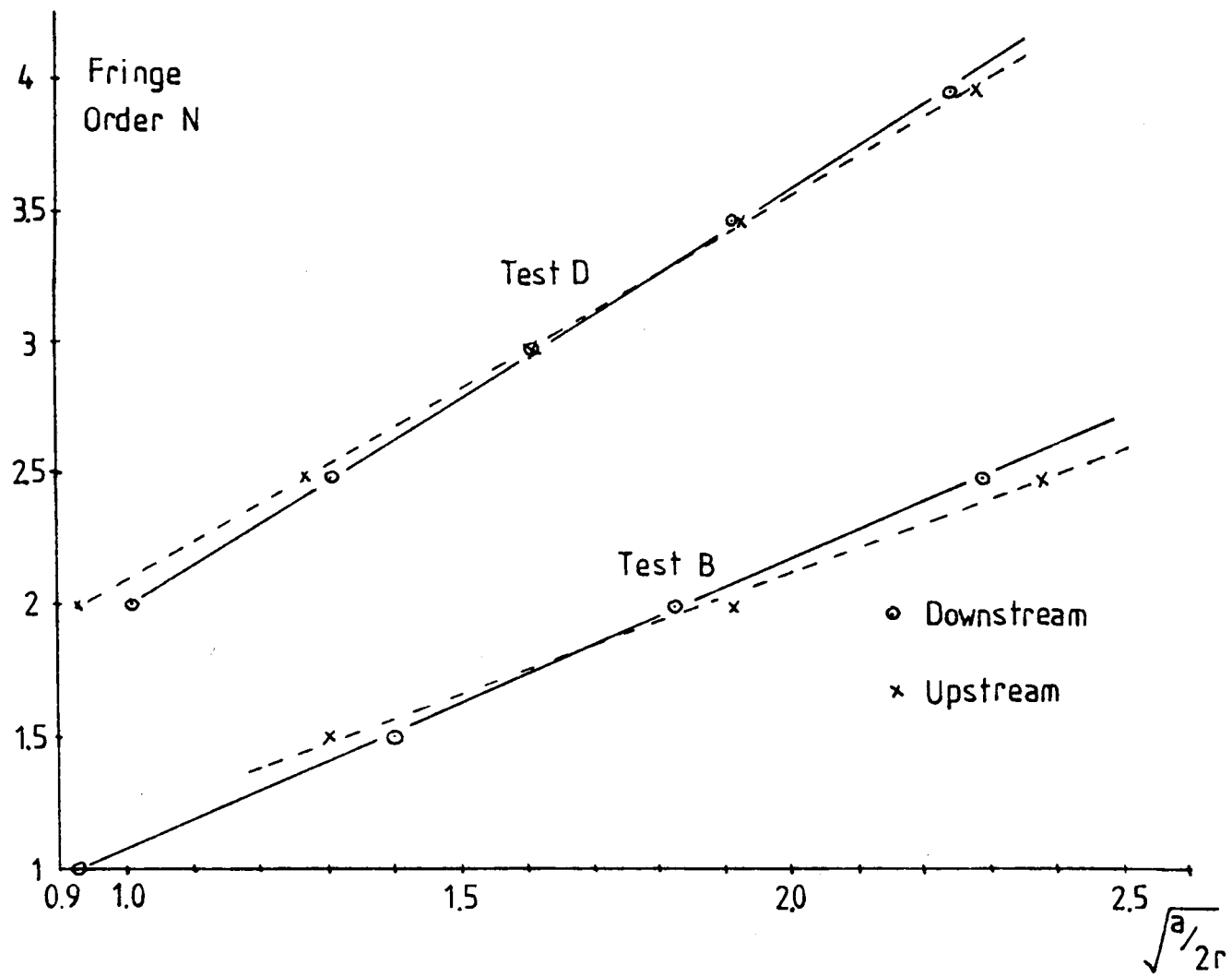


Fig. 5.15 Typical plots of N v. $\sqrt{\frac{a}{2r}}$ for DNB test.

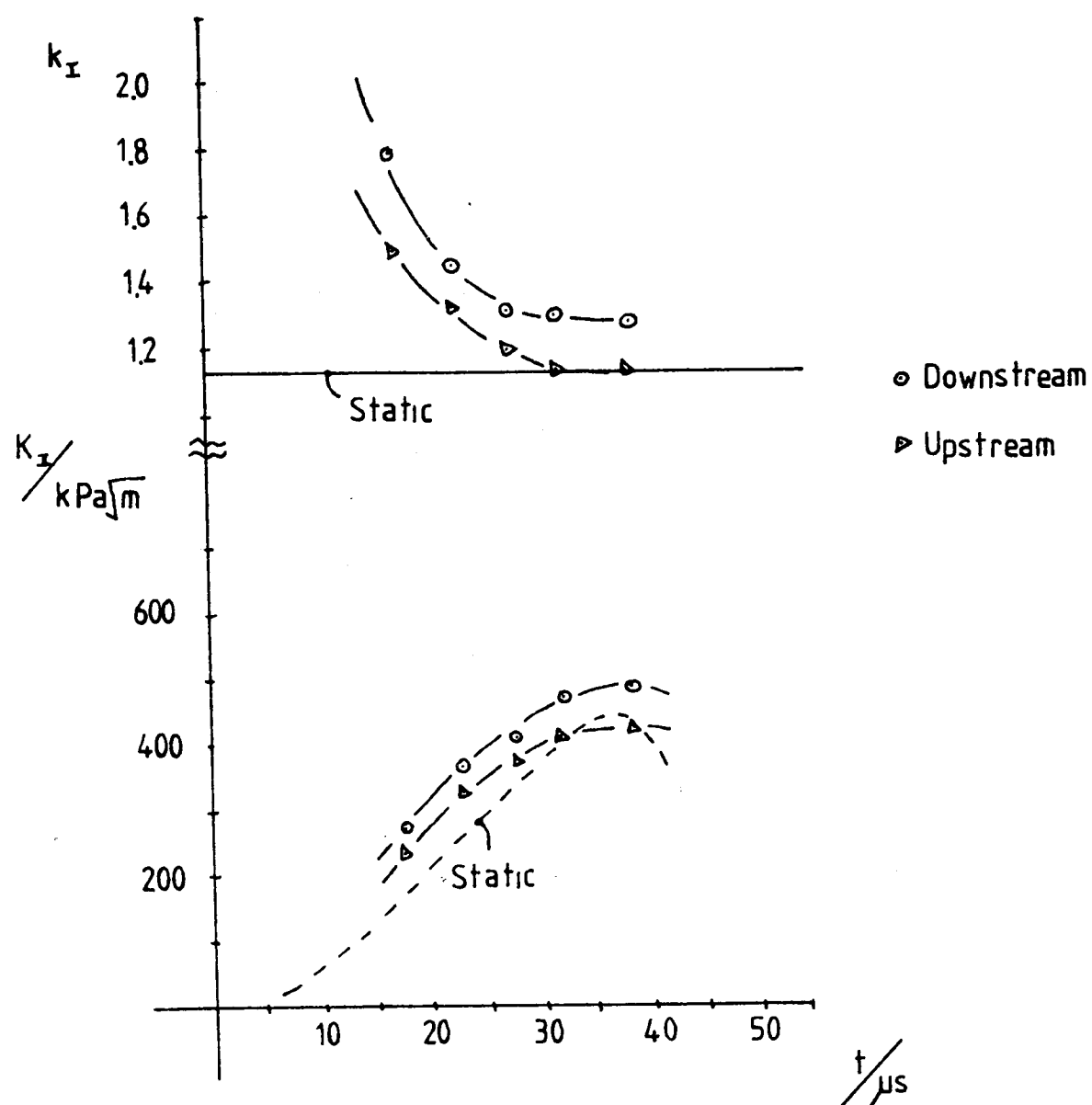


Fig. 5.16 Variation of $K_I(t)$ and $k_I(t)$ for DNB subject to ramp loading.

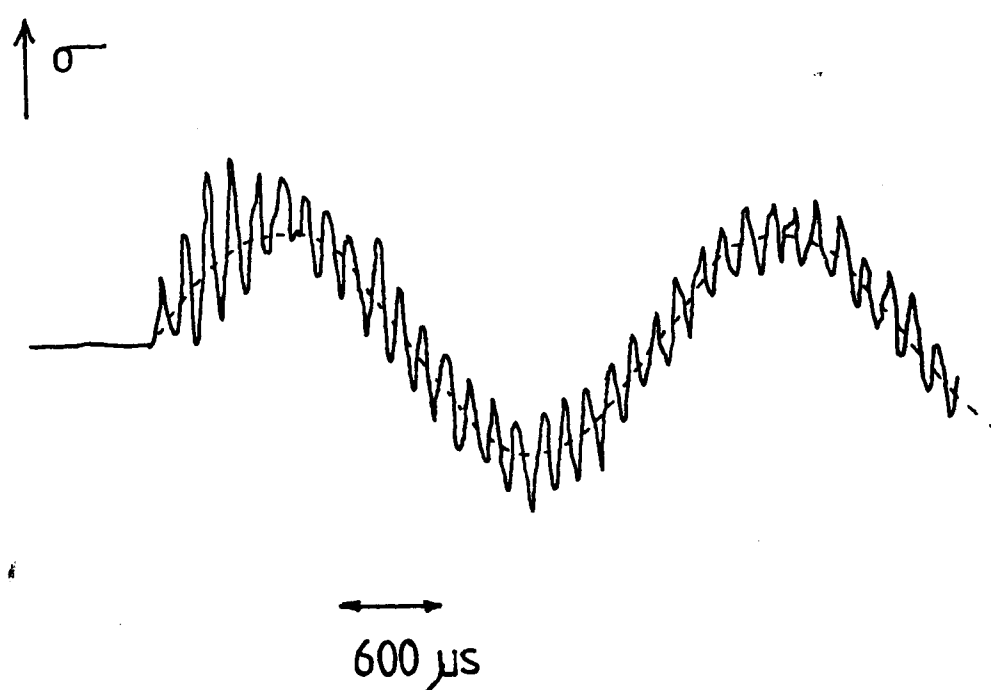


Fig. 5.17 Response of specimen and tup after large time.

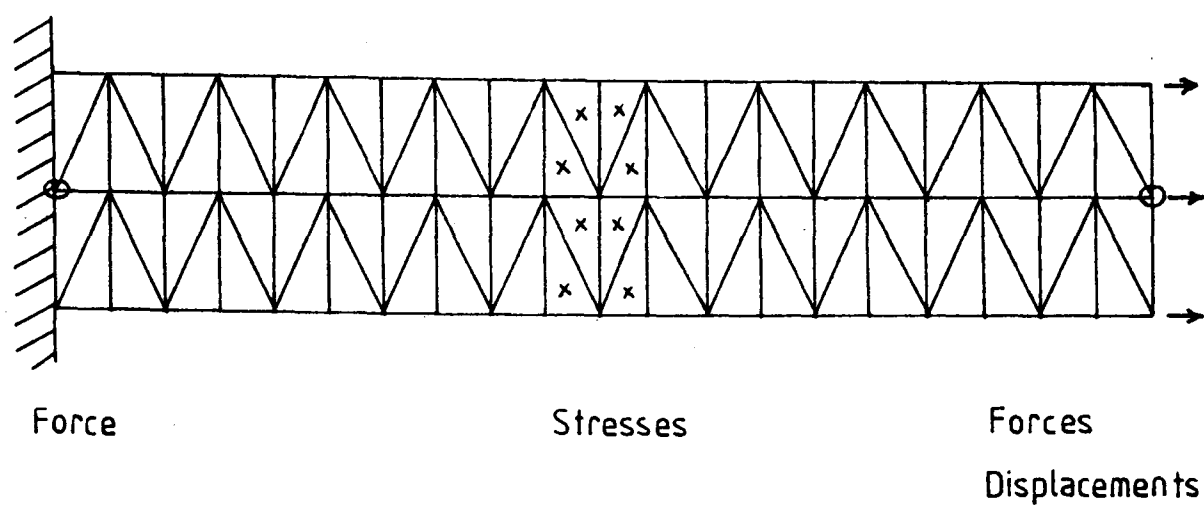


Fig. 5.18 Discretization of unnotched bar.

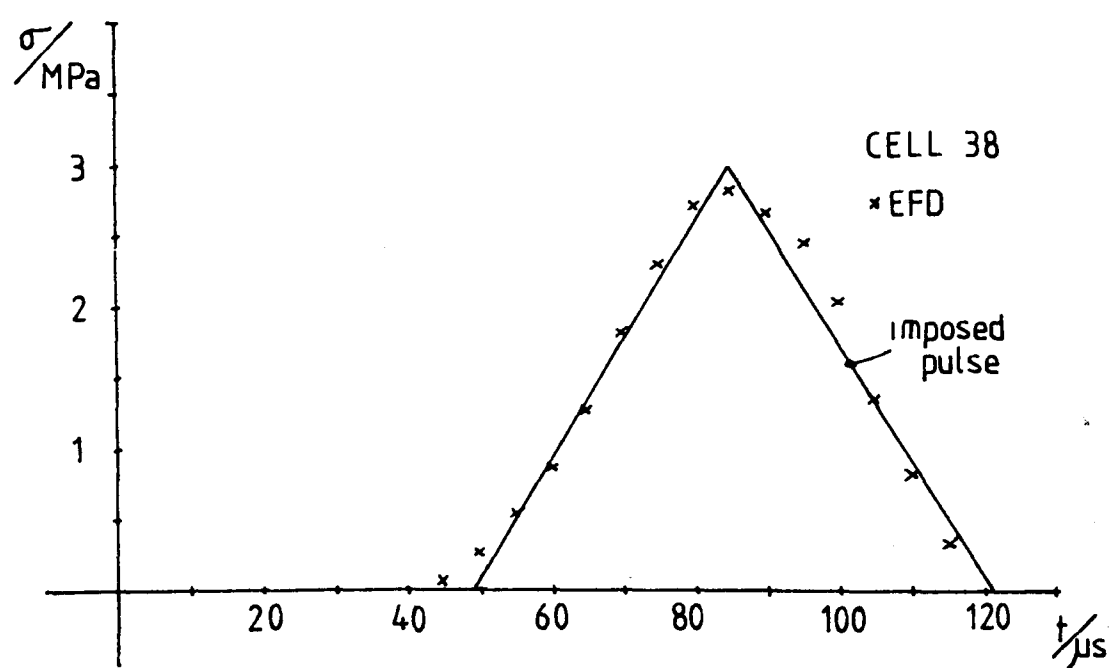
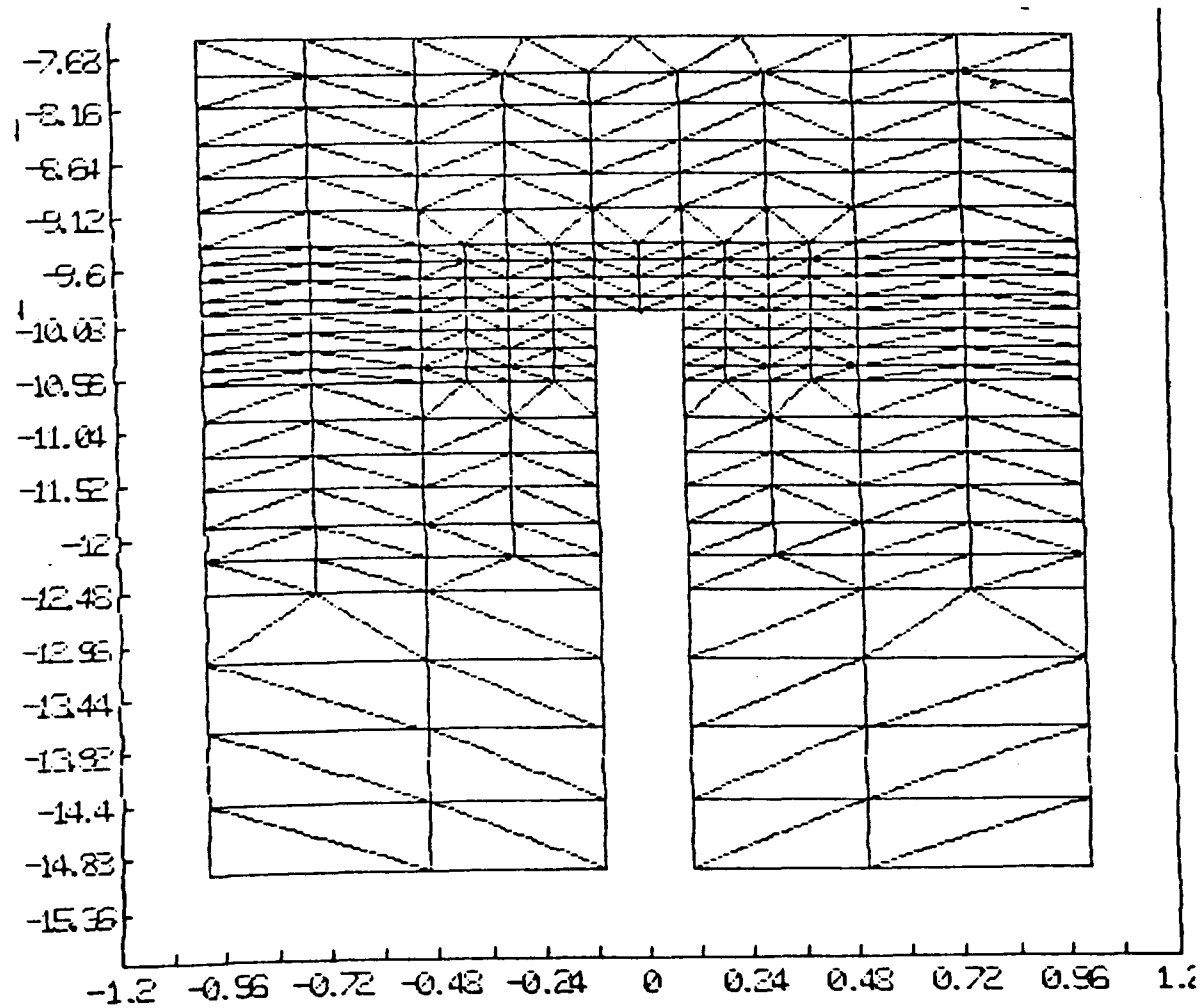
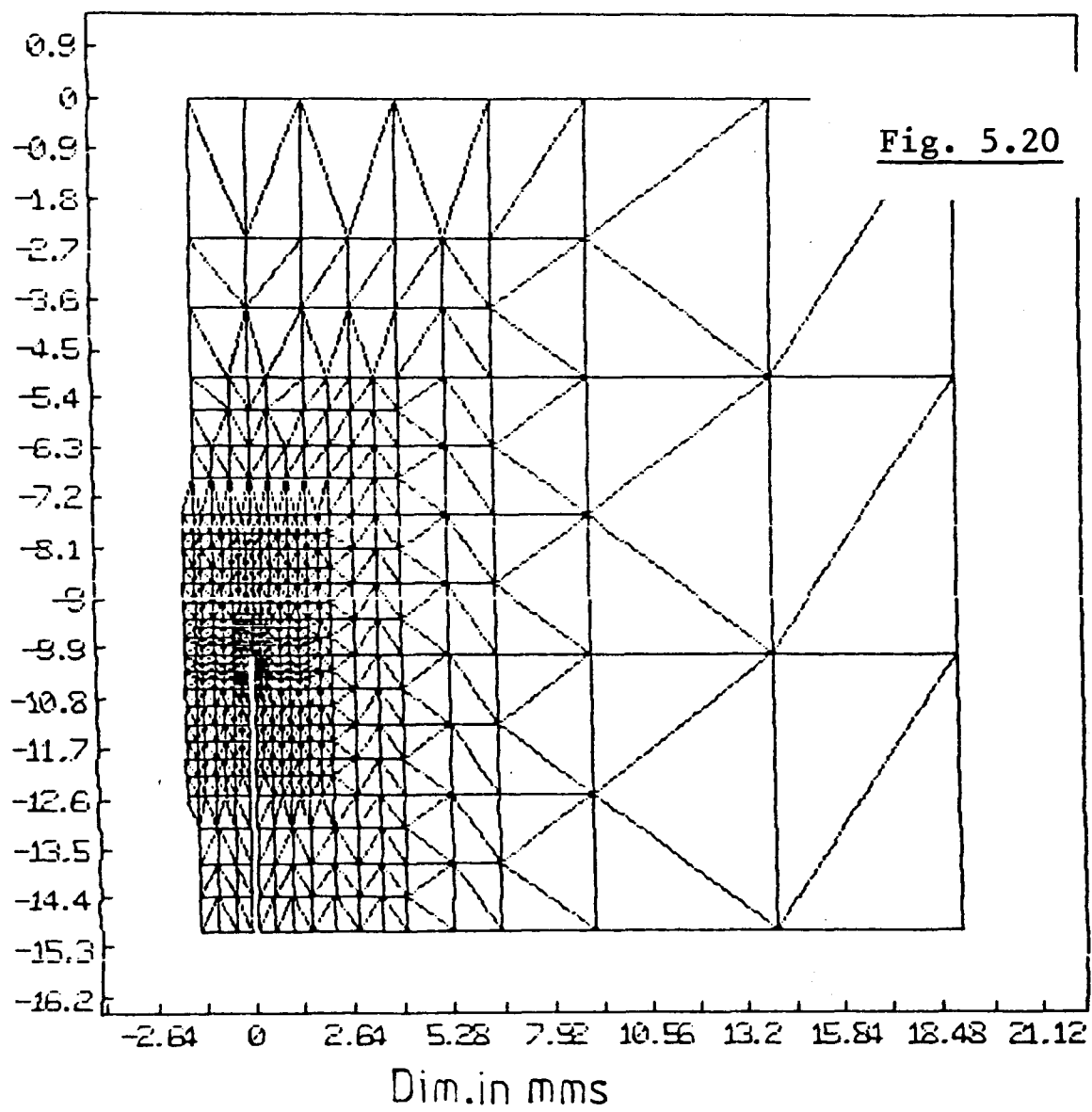
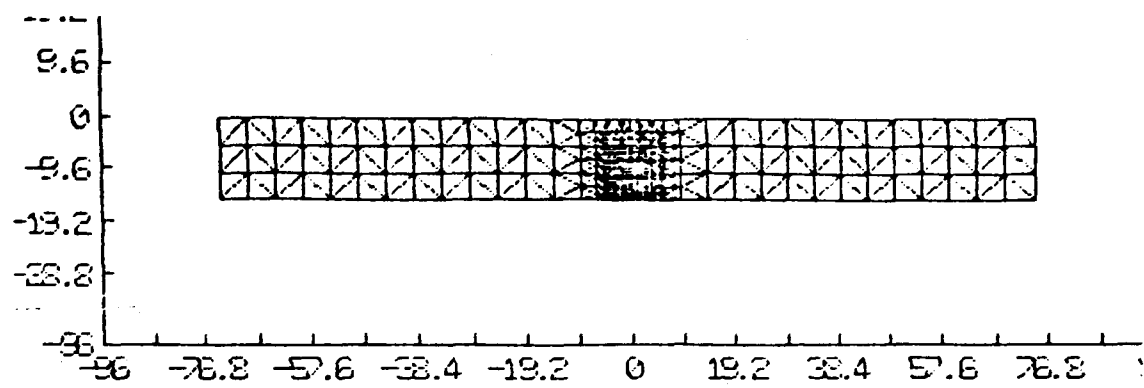


Fig. 5.19 Comparison between imposed pulse and numerically modelled pulse for central cell.



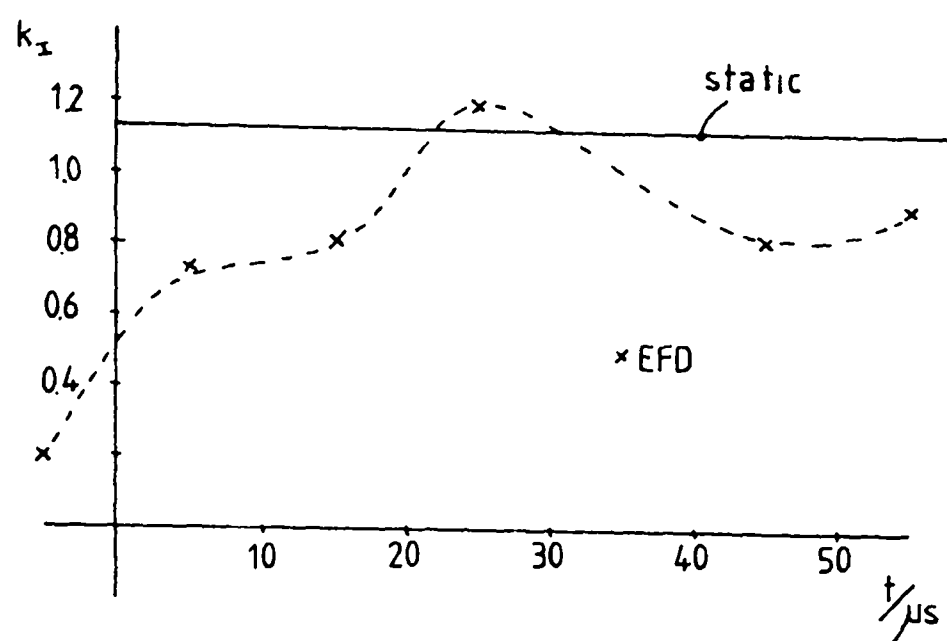


Fig. 5.21 Numerical response to step load.

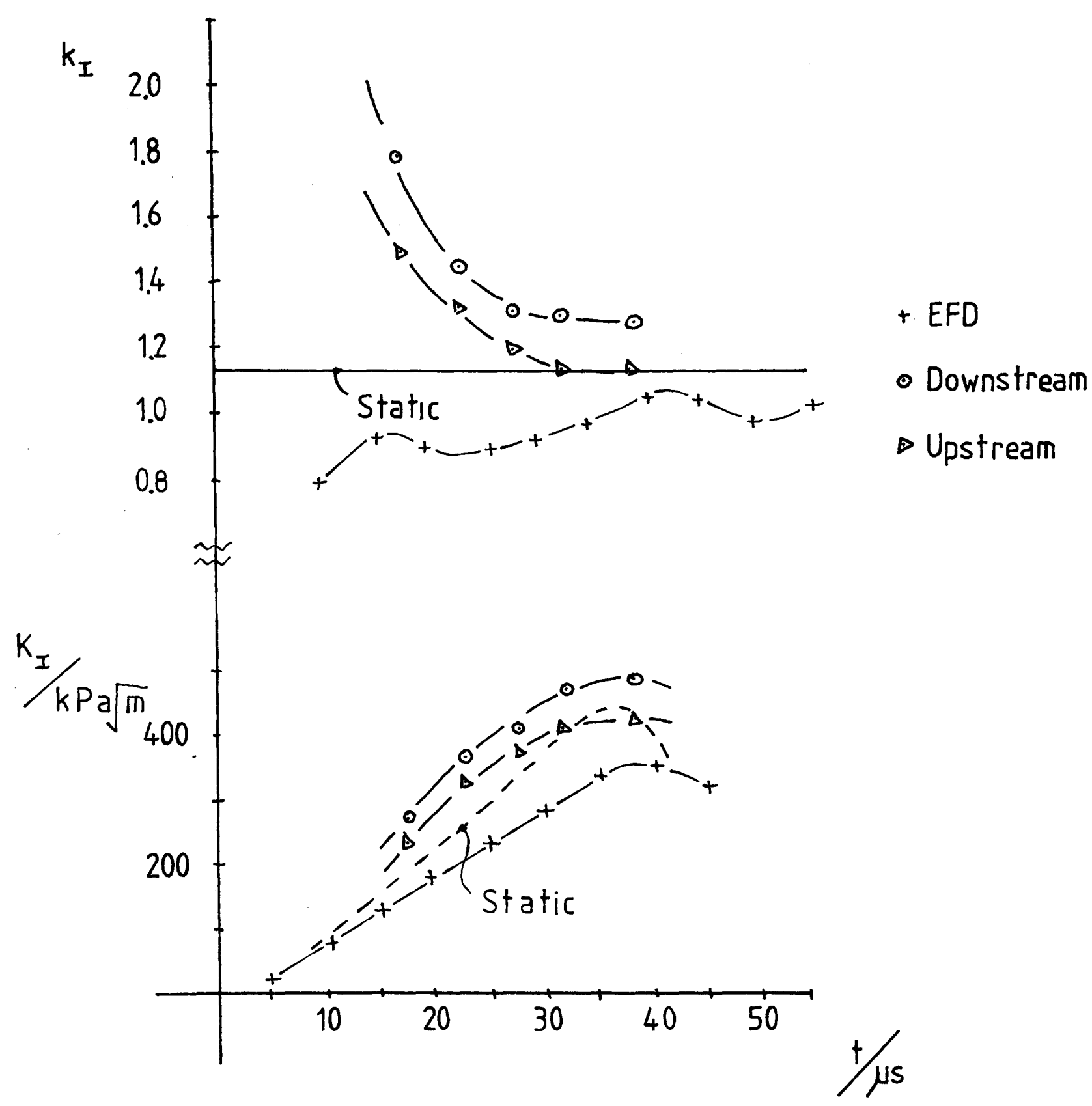


Fig. 5.22 $K_I(t)$ and $k_I(t)$ for notched bar subjected to a triangular pulse.

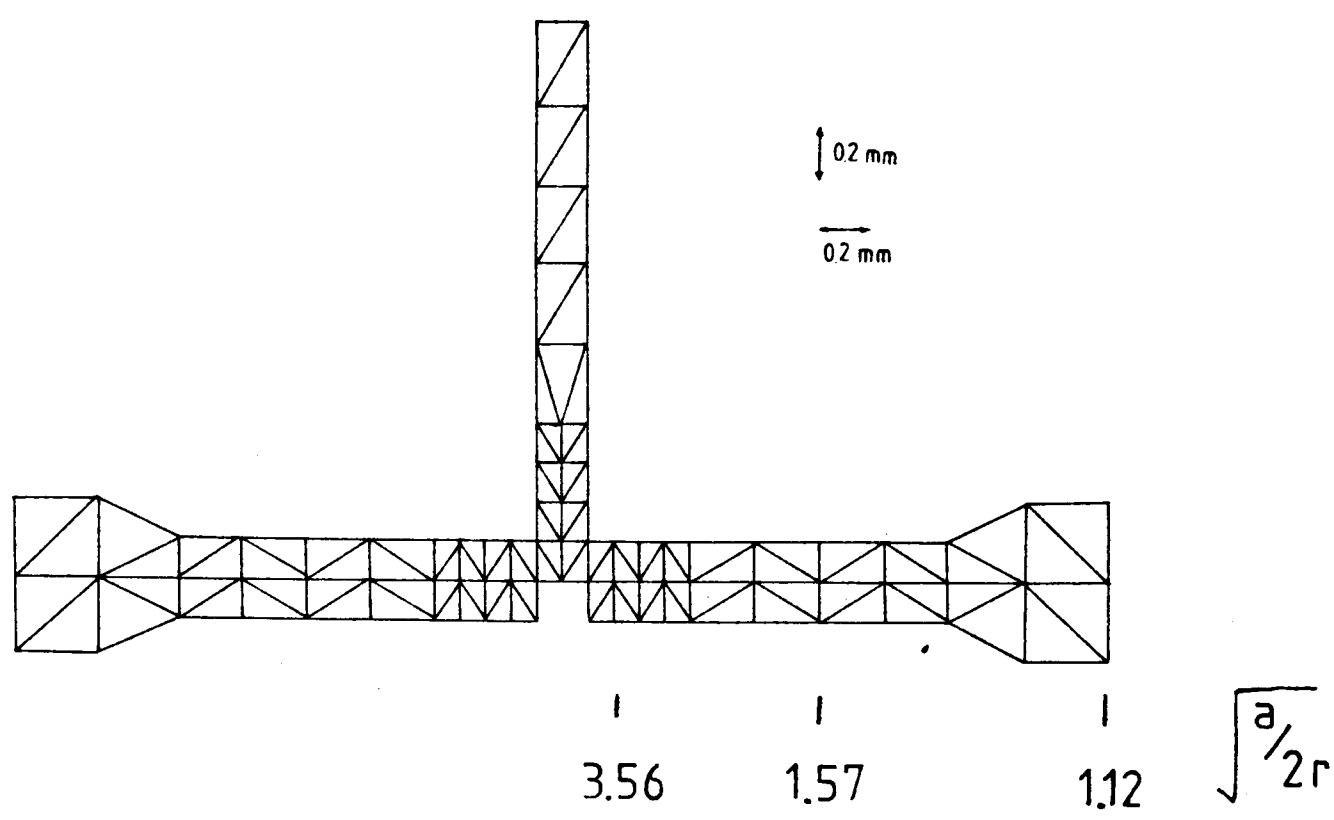


Fig. 5.23 Discretization, in vicinity of the notch, used for data reduction.

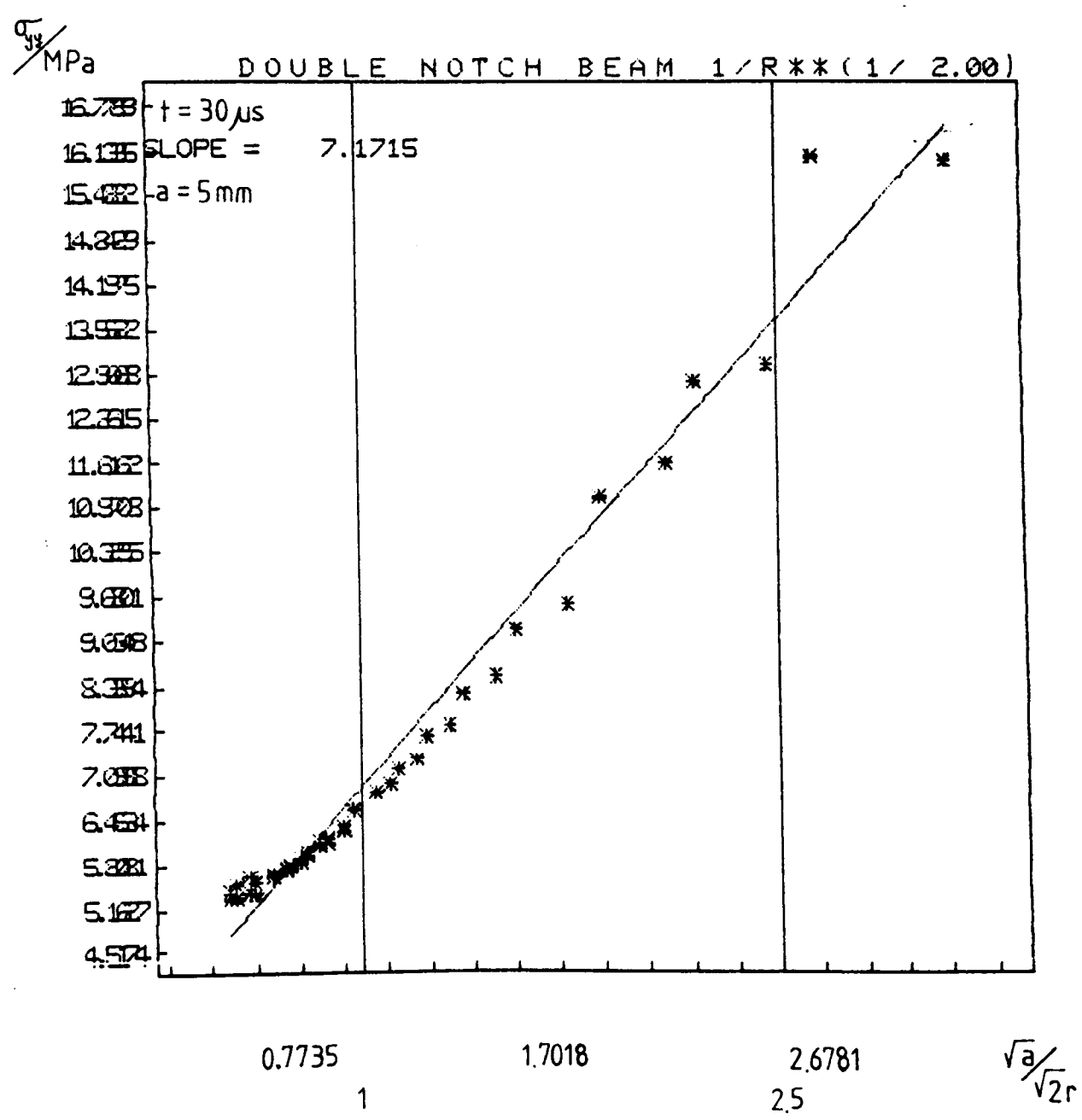


Fig. 5.24 Plot of $\sigma_{yy} \sqrt{a/2r}$ at $\theta = 0^\circ$.

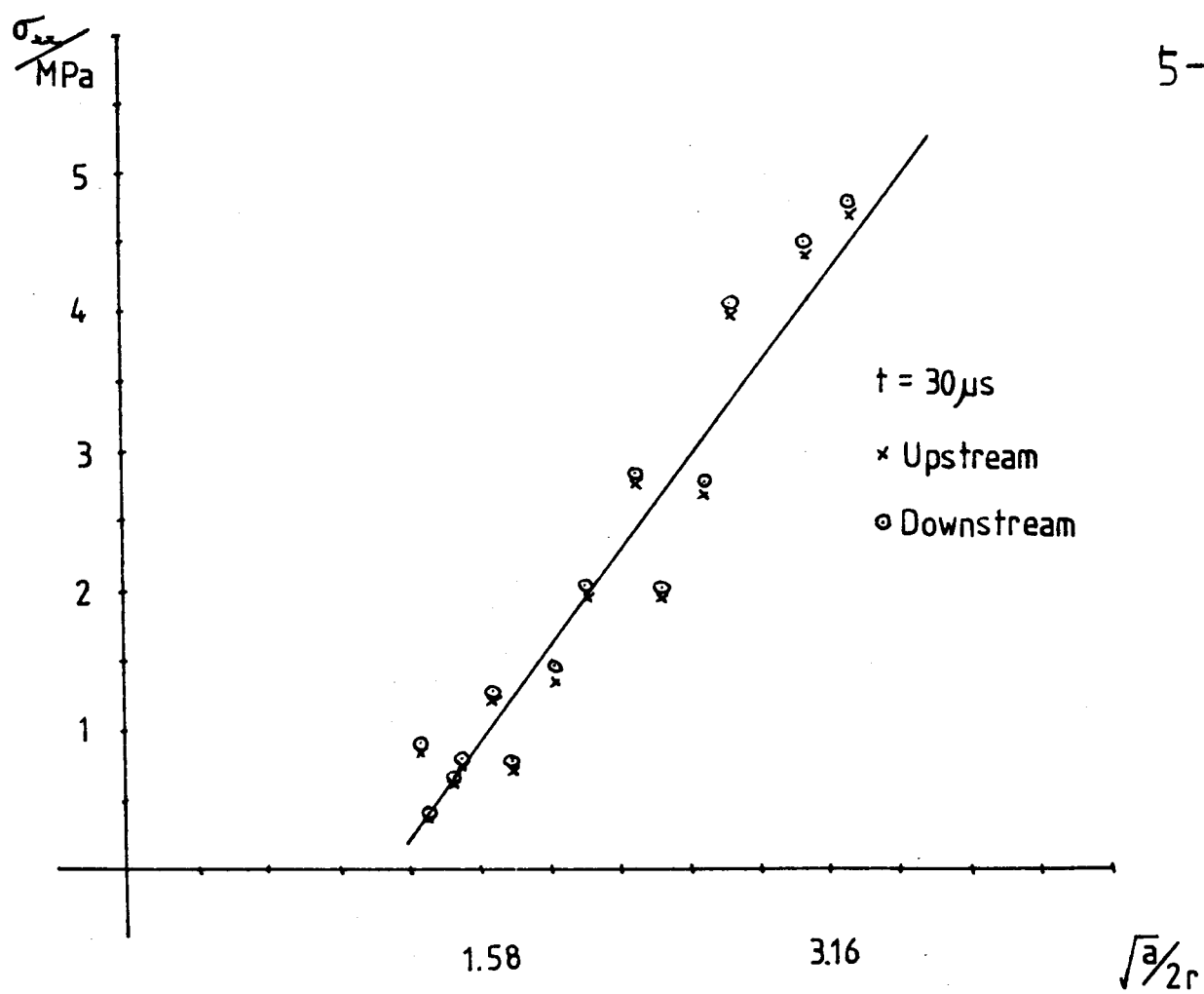


Fig. 5.25 Plot of σ_{xx} v. $\sqrt{a}/2r$ at $\theta = 90^\circ$.

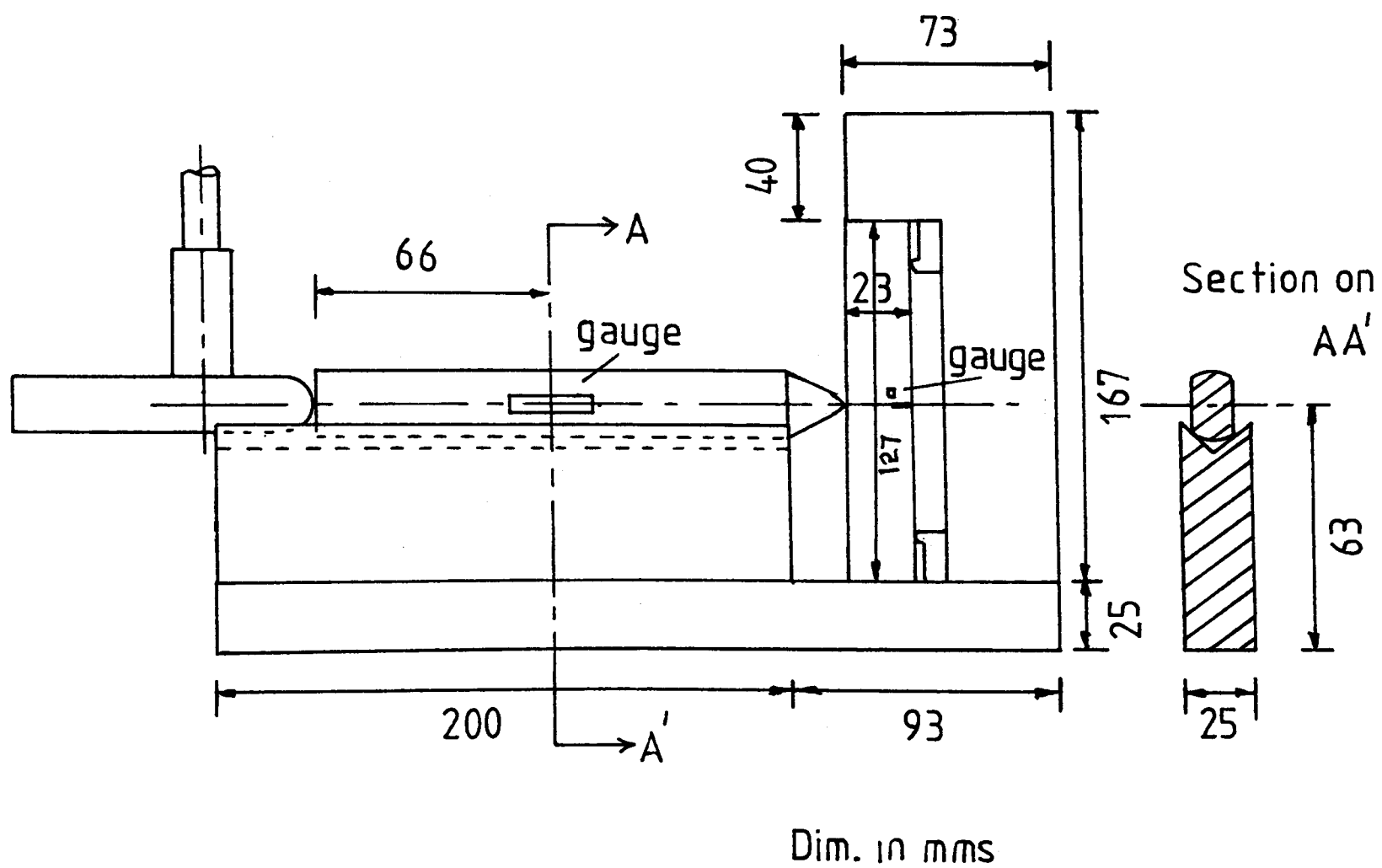


Fig. 5.26 Loading mechanism for the Charpy geometry.

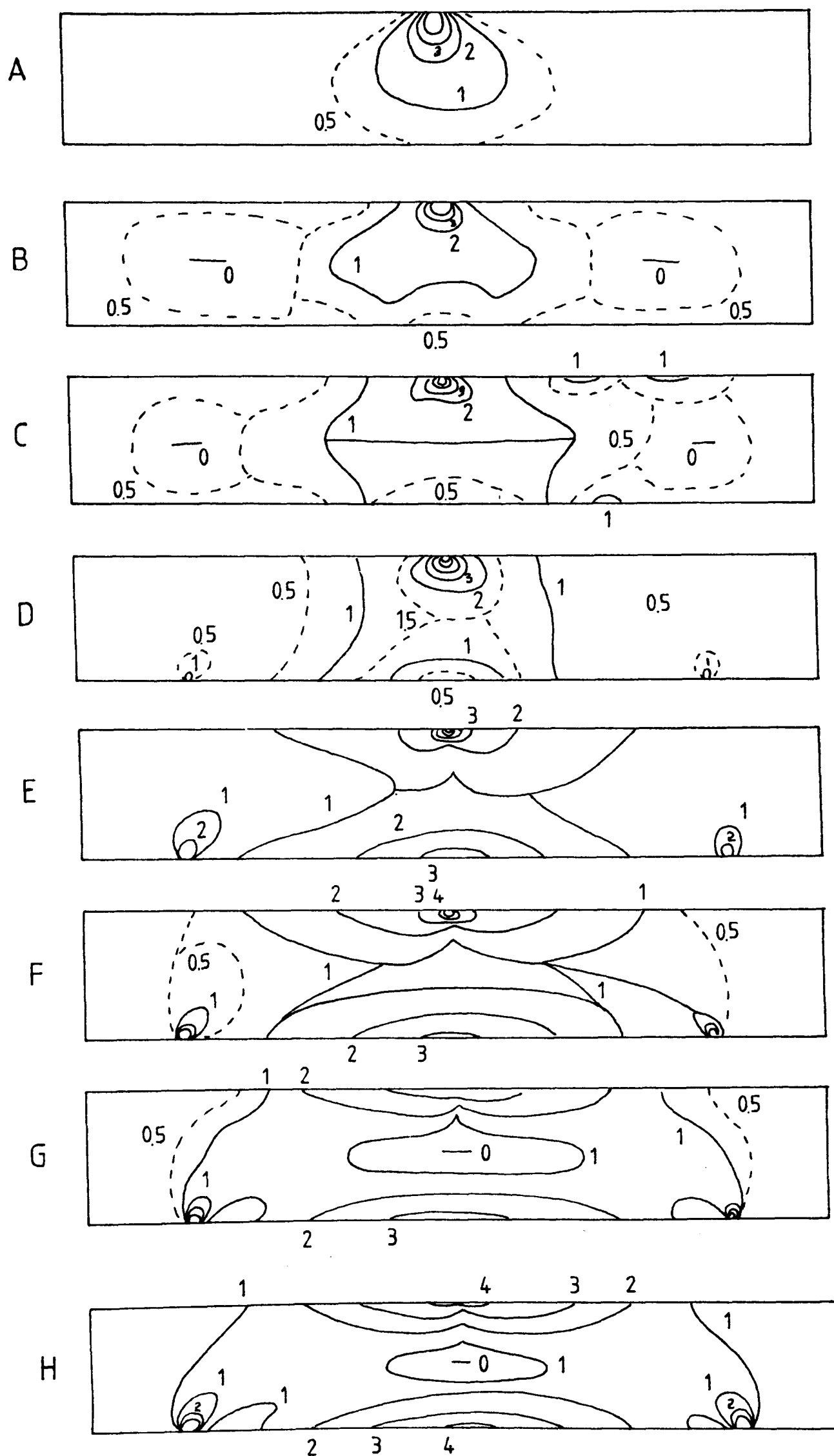


Fig. 5.27 Growth of fringes for the unnotched bar.

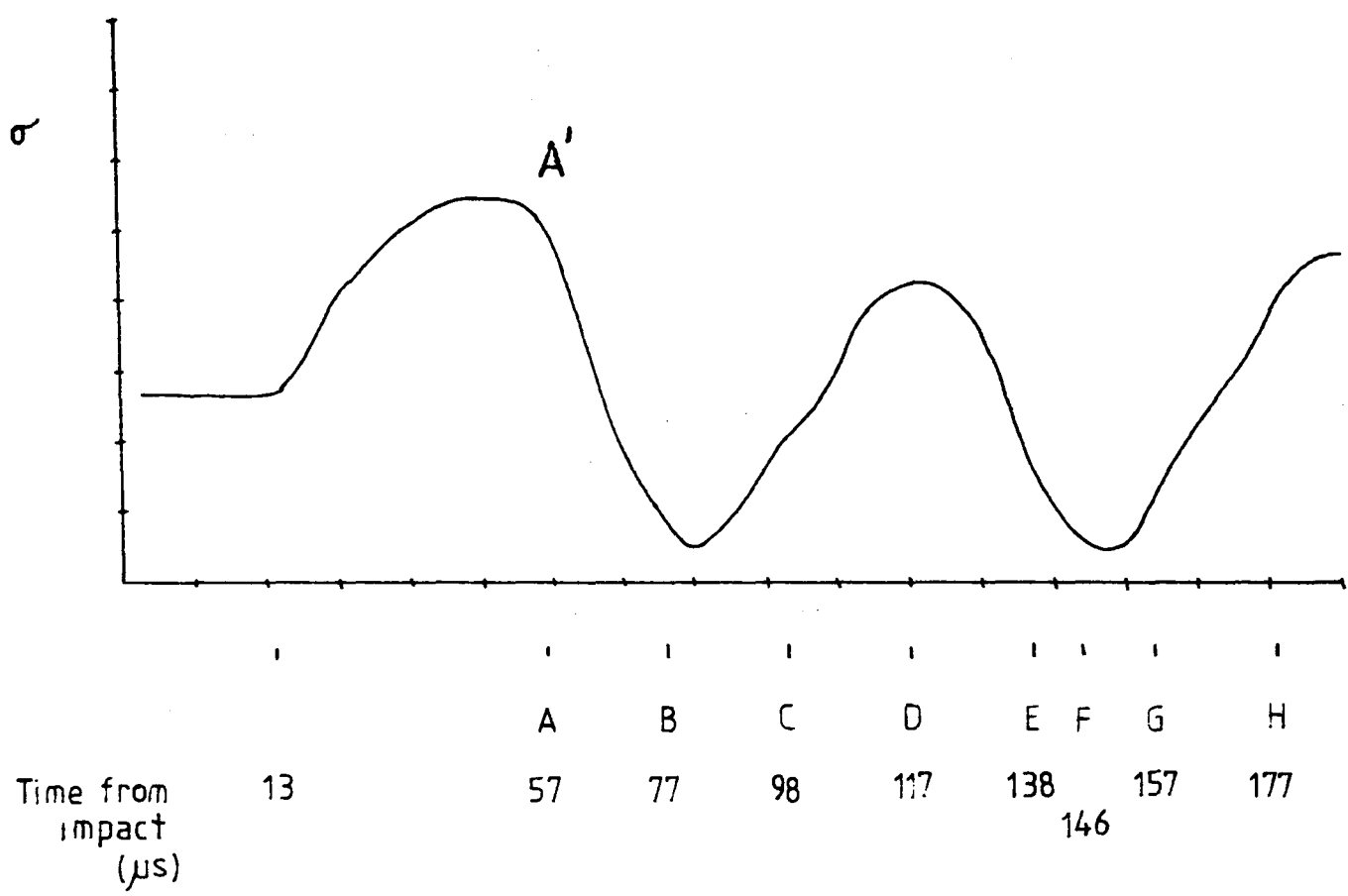


Fig. 5.28 Times of photographs with respect to the stress level in the Charpy tup.

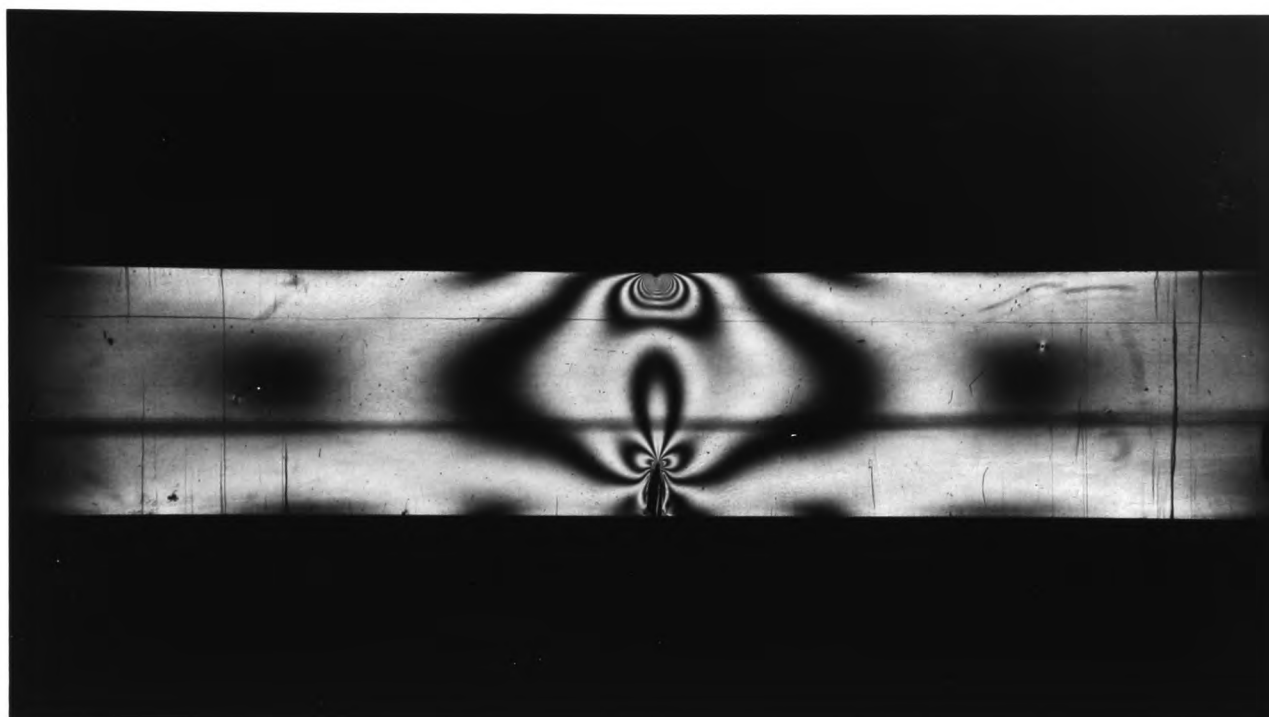


Fig. 5.29 Photograph of notched bar at $t = 98 \mu s$.

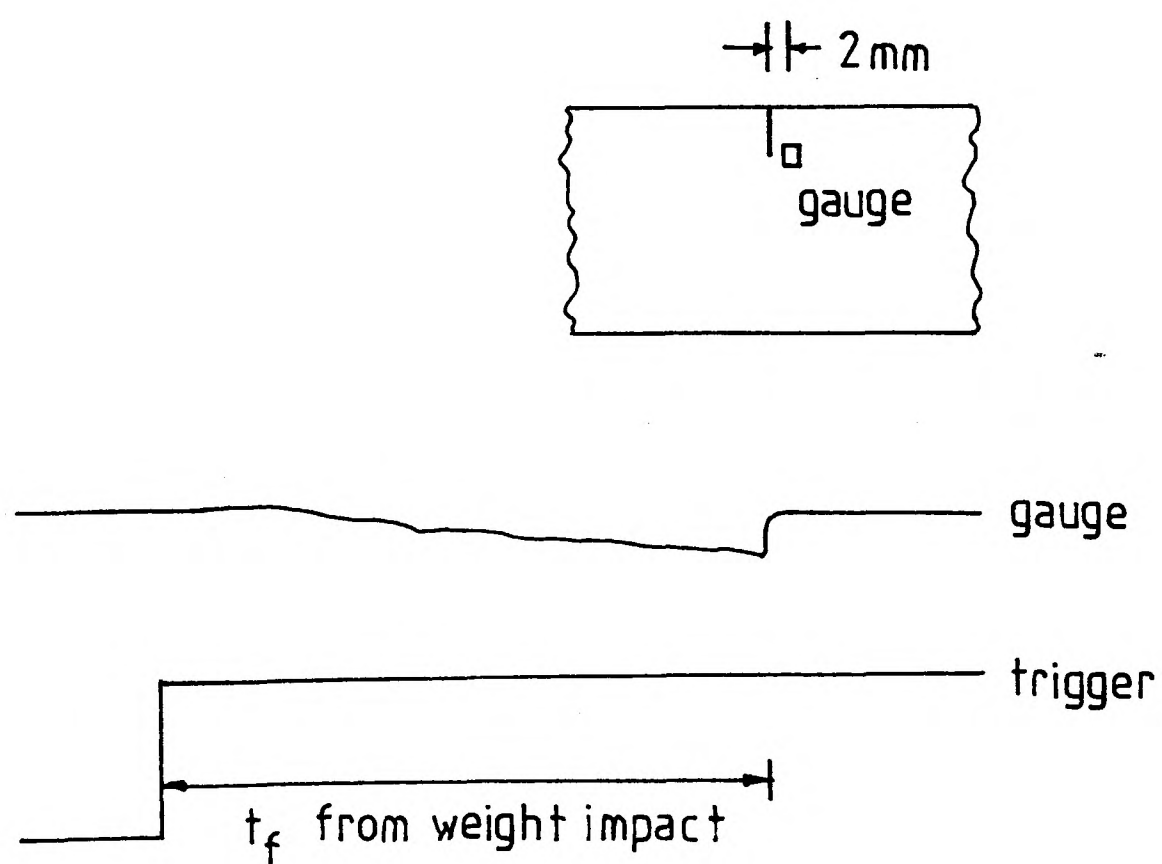


Fig. 5.30 Crack gauge trace for the Charpy test.

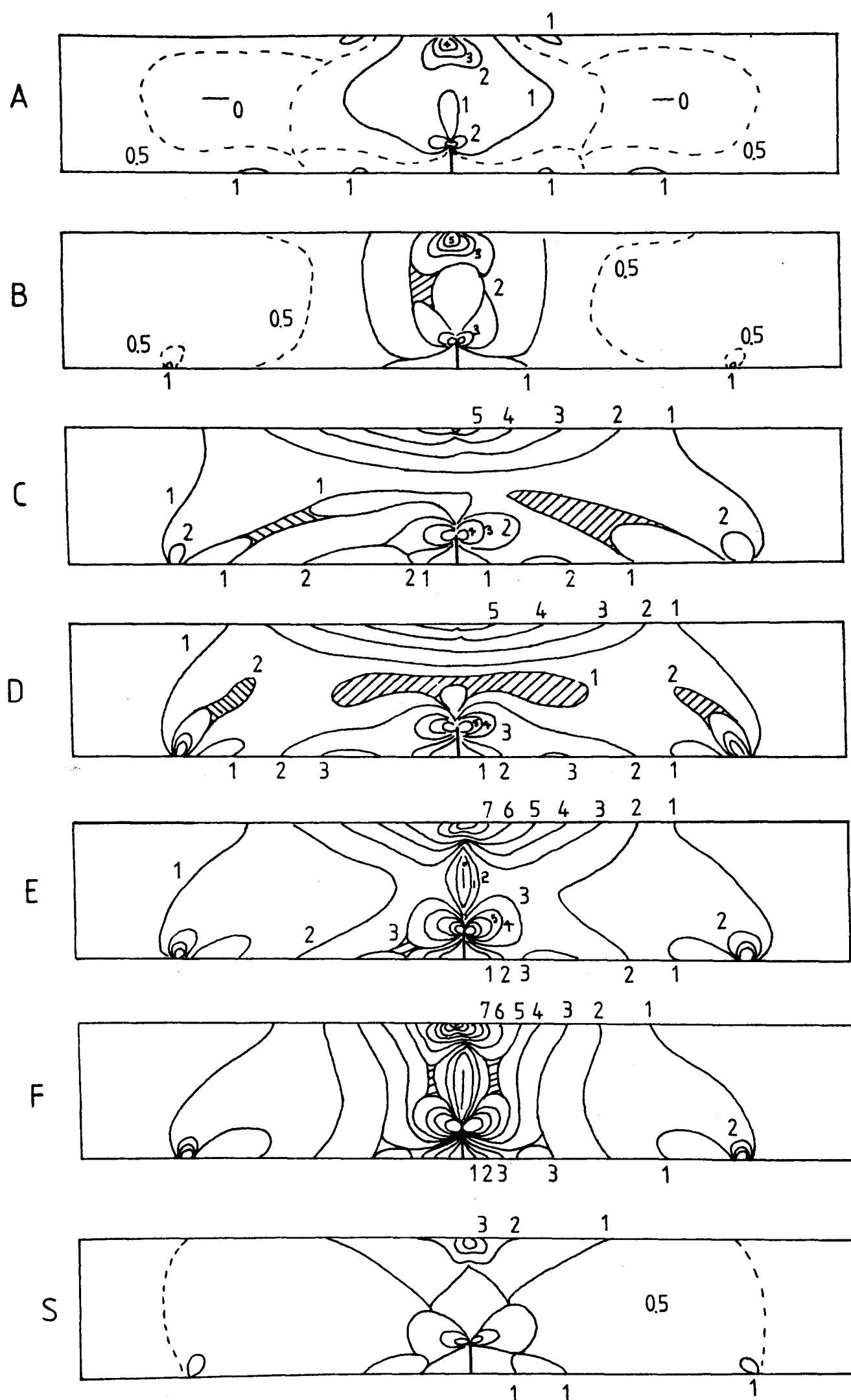


Fig. 5.31 Growth of fringes for the Charpy test.

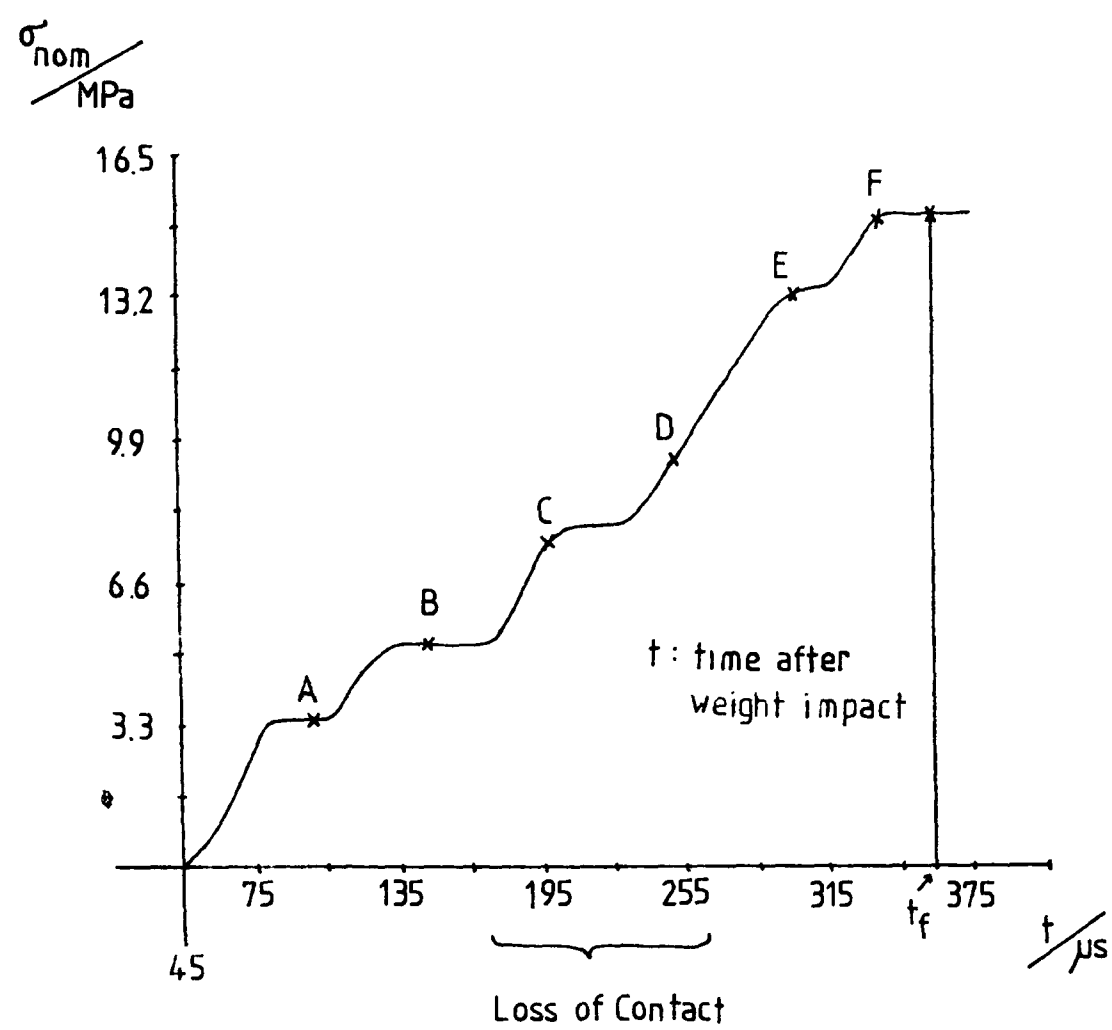


Fig. 5.32 The measurement of nominal stress for the Charpy test.

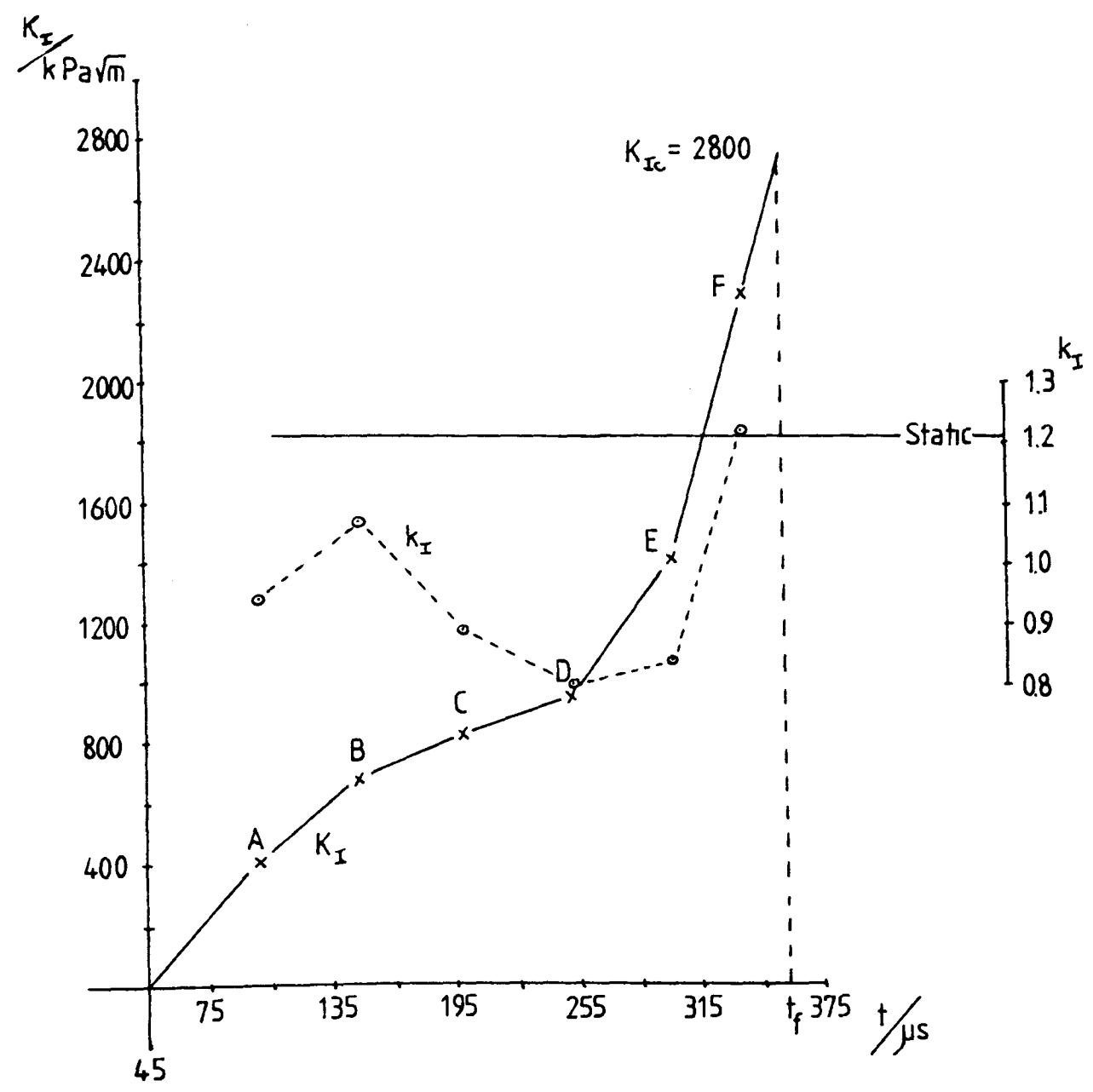


Fig. 5.33 Plots of $K_I(t)$ and $k_I(t)$ for the Charpy test.

CHAPTER VI

INSTRUMENTED IMPACT TESTING OF THE CHARPY AND DNB GEOMETRIES

6.1 INSTRUMENTED IMPACT TESTING OF THE CHARPY GEOMETRY USING THE HOPKINSON PRESSURE BAR AND THE NITROGEN GAS GUN

6.1.1 Introduction

From Chapters III and V it was concluded that the standard Instrumented Charpy Machine behaves in a complex manner, mechanically. The dynamic response, and hence dynamic *calibration*, of the specimen also depends on the impactor and supports. Given the diversity of impact testing machines it is difficult to see how a general measure of the dynamic response of the Charpy test can be attained. Thus the options are either to specify the Charpy test and equipment much more closely, or to look for a more unequivocal loading system or to look for an alternative geometry that is not subject to so many variables. The latter two options will be addressed in this Chapter.

The first aspect to be addressed is the derivation of a loading system that yields more accurate data and is less variable. The Split Hopkinson Bar is a standard piece of equipment used by researchers in the field of materials at high rates of strain, and is well described in [1]. Typically the apparatus consists of three collinear 1" diameter steel bars 2 m in length^[2,3]. An outer bar is fired at the middle bar, and a one-dimensional pulse travels away in both directions from the impact interface. The specimen is placed at the far end of the middle bar. The stress pulse is incident on the specimen and waves are reflected and transmitted. From strain gauges mounted on the bars, measuring incident, reflected and transmitted loads, the forces applied to the specimen can be measured. The

Hopkinson Pressure Bar does away with the output bar and hence only the applied load to the specimen can be measured. Nicholas^[1] has proposed the use of the Hopkinson Pressure Bar for instrumented Charpy testing and this method will be used here.

6.1.2 The Nitrogen Gas Gun

6.1.2.1 Introduction

A small capacity gun already existed at Oxford, previously being used for plate impact work, and is described in [4]. Fig. 6.1 gives a general view of the apparatus adapted for Instrumented Charpy Testing. The reservoir, volume 0.2 dm^3 , was typically pressurized at 250 psi giving a projectile velocity of 7.5 ms^{-1} . The projectile is a 0.75 m long silver steel bar of 15 mm diameter. The projectile was released using a clamping mechanism at the reservoir end actuated by pistons^[4]. The barrel was machined from a length of $5/8$ " internal diameter heavy gauge tube. The entire gun assembly was mounted on a massive channel section.

6.1.2.2 Loading System and Supports

Fig. 6.2 gives a schematic of the projectile, the intermediate tup, specimen and supports. The lengths of the bars were constrained by the existing apparatus. Appendix C gives a description of the stress wave behaviour of a similar system, described in the next part of this Chapter. The appendix shows how, from traces from strain gauges S1 and S2, the applied force can be derived. In the case of the Nitrogen Gas Gun the maximum pulse duration is 160 μs . The pointer shape is non-standard^[5] as are the supports (see Fig. 6.3). Both the pointer and supports were hardened to guard against damage.

6.1.3 Electronic Instrumentation and Conduct of a Test

The instrumentation is described in detail in the next part but Fig. 6.4 gives the specific set up for the Nitrogen Gas Gun tests. Strain gauge systems S1 and S2 were in the form of two 120Ω metal

foil gauges, gauge length 6 mm and gauge factor 2.13, wired up to compensate for bending (see Fig. 6.5). The strain gauges were part of a 5 V D.C. Bridge Circuit. Signals were differentially amplified and stored on 20 MHz transient recorders. Strain gauges were recessed on the bar for protection.

To conduct a test the gun was loaded, instrumentation primed and gun fired manually. The latter was required to prevent spurious triggering of the instrumentation. The transient recorders were triggered by the rising pulse from strain gauges S1.

6.1.4 Specimen Material and Preparation

Three specimens only were tested in this apparatus namely Silver Steel hardened to R_c 42, Silver Steel hardened to R_c 60 and DIN 17115 Chain Link Steel hardened to R_c 43 (see Table 6.2). Table 6.1 gives the compositions of these two materials. In the case of the Silver Steel specimens, which were cut from a 10 mm x 10 mm bar, the notches were broached prior to heat treatment. The preparation of the DIN steel specimen is described in Section 6.2.4.

6.1.5 Results, Discussion and Conclusions

The three results are shown in Fig. 6.6 and given in Table 6.3. Note that due to the stress wave behaviour of the pointer results are suspect for $t < 10 \mu s$ (see Appendix C). In the case of the two brittle specimens a 1 mm metal foil gauge, gauge factor 2.15, was placed adjacent to the notch tip to measure crack initiation (see Fig. 6.4). The resultant traces were similar to that for Araldite (see Fig. 5.30) and hence the time of crack initiation could be located accurately. Specimen A fractures within the suspect period of $t < 10 \mu s$. Specimen B fractures after the maximum load has been achieved - this phenomenon has been highlighted for brittle materials by Kalthoff et al.^[6]. In the case of the more ductile specimen the rupture process takes much

longer and plasticity occurs prior to failure. A strain gauge adjacent to the notch tip is difficult to interpret for crack initiation, due to plasticity, but the applied load gives a good indication of material rupture (see Fig. 6.6).

The values of force were obtained using the strain gauge factors directly (see Section 5.1.4.1) - this method has been found to agree closely with actual calibrations (see Section 6.2.3). The impact velocities are shown in Fig. 6.6 - these can be derived from stress levels using one-dimensional wave theory^[7], i.e.

$$v_2 = \frac{\sigma}{\rho c}$$

where v_2 is the initial particle velocity and is half the impact velocity. It is shown in Appendix C that the speed v_3 , at which the specimen is forced apart, is dependent on the applied force. An average value of v_3 can be taken when plasticity occurs. It can be seen that the impact velocities are higher than those associated with the Instrumented Charpy Test, viz. $3 \text{ ms}^{-1} < v_{\text{imp}} < 6 \text{ ms}^{-1}$ ^[5]. The period of apparent oscillation for the Charpy 'V' notch specimen is given in Table 3.1, and is of a value of $30 \text{ } \mu\text{s}$. In the case of test C inertial oscillations have a period of $45 \text{ } \mu\text{s}$. The maximum loads for all three tests vary from 20 kN to 29 kN.

In the case of specimen C fracture takes place at $t = 132 \text{ } \mu\text{s}$ - this is close to the unloading time for the tup, i.e. $t = 160 \text{ } \mu\text{s}$.

Thus for more ductile specimens the period of analysable data would run out before the specimen fractured. Thus a longer tup was required. Also there was a requirement to reduce the velocity of impact whilst retaining similar forces and so a larger diameter tup was required. For these reasons the larger gas gun described in^[2,3] was adapted.

6.2 INSTRUMENTED IMPACT TESTING OF THE CHARPY GEOMETRY USING THE HOPKINSON PRESSURE BAR AND AIR GUN

6.2.1 Introduction

A number of years ago the apparatus was converted from a supersonic blowdown wind tunnel to a Split Hopkinson Bar Apparatus. The apparatus was then used for dynamic fracture toughness measurements on CTS specimens^[2,3]. To adapt it for Charpy testing the output bar was dispensed with and the loading bar reduced from 1" diameter to 19 mm diameter. A schematic of the apparatus is given in Fig. C.1. The use of 2 m long bars meant that pulse lengths of 800 μ s could be achieved. The diameter of the bar had to be reduced to avoid jamming of the specimen between the supports.

As no detailed description of the apparatus has been published, but see [8], an extended discussion is given below.

6.2.2 The Mechanics of the HPB Facility

The size of the gun, including the reservoir and dump tank, is large, namely 20 m in length with the barrel being 10 m in length. The 2 m long, 1" diameter silver steel impact bar is run up and down the barrel, of 69.9 mm internal diameter, on nylon rings using a system of air pumps and valves. A nylon piston head is attached to the impact bar at the breech end. The loading bar is held half in and half out of the barrel, thus impact takes place inside the barrel whilst the specimen is outside the barrel^[2]. The loading bar is supported on nylon rings inside the barrel and by saddles, suspended between two parallel bars, in the working section. A schematic of the two bars, and the instrumentation, is given in Fig. 6.7. In the case of the HPB tests the loading bar is arrested by the now redundant output bar, with a copper head attached to it.

To fire the gun the impact bar is manoeuvred down to the breech end using a vacuum pump. The barrel is then pressurised or

evacuated differentially at either side of the piston. For example with the reservoir at 300 mm Hg vacuum and the barrel at 30 mm Hg vacuum the bar can accelerate to 2 ms^{-1} prior to impact. Thus it can be seen that the capacity of the gun is totally under used - the maximum possible reservoir pressure is 6000 psi. At these low velocities speeds can lack reproducibility. The gun is fired manually by opening a ball valve, to guard against premature triggering of the instrumentation.

6.2.3 Supporting Instrumentation

The data reduction scheme for the HPB is given in Appendix C - the loading bar is strain gauged at either end. Four strain gauges are used at each position to eliminate bending and to provide stability for calibration (see below). The bridge layout is given in Fig. 6.9. The gauges are wired into the bridge in such a way as to avoid spurious signals due to a 'pinch' effect, resulting from the passage of a stress wave. A two gauge system (see Section 6.1.3) was used to trigger the instrumentation (see Fig. 6.7). In all the above cases 6 mm long metal foil gauges of gauge factor 2.13 were used. Each strain gauge system was calibrated by loading the bar statically, using an hydraulic jack, and measuring the output from the bridge on a μV meter. This calibration gave an average of $24.6 \mu\text{V}/\text{MPa}$ which compares with $25.3 \mu\text{V}/\text{MPa}$ using the theory given in Section 5.1.4.1.

A photograph of the instrumentation is given in Fig. 6.8. The signals from strain gauge systems S1 and S2 were amplified using high quality differential amplifiers (see specification in Appendix E). These were required to convert the mV signals into volt signals and to filter out interference effects induced in the long coaxial screened leads attached between the gauges on the bars and the bridge box. The amplifiers could only be run up to 2V peak to peak to avoid problems of slew rate. A stress pulse travelling down a bar has a $20 \mu\text{s}$ rise

time, which includes the dynamic response of the 6mm foil gauges. The slew rate of the amplifiers is $0.5 \text{ V}/\mu\text{s}$, thus the maximum amplifier output is $10 \text{ V}^{[8]}$. The amplified signal was then stored in a 20 MHz transient recorder (see specification in Appendix E). The dynamic response and sampling rate of these high quality recorders were more than adequate. The amplifier and transient records were calibrated by inputting a 2 mV or 5 mV step pulse and measuring the output on the X-Y plotter.

The transient recorders store data as a result of a single sweep and hence they need to be triggered accurately. Triggering on the rising pulse of gauges S1 was not reliable enough and so a triggering system was developed (see Fig. 6.7). The amplified signal from the trigger gauges was fed into a comparator circuit given in Fig. 6.10. The 531 Op-Amp was an attempt to bypass the need for a Fylde amplifier, but it would seem that an integral differential amplifier, encased as a unit, is required. The 311 comparators operate in 40 nS to give a voltage step dependent on the applied voltage. The first comparator gives a +V to 0V step and the second a 0V to +V step. Thus when the input rose above a preset reference voltage, the TTL compatible step occurred. This step was used to externally trigger the transient recorders.

6.2.4 The Impactor, Specimens and Supports

Details of the pointer, for transferring the load from the bars into the specimen, are given in Fig. C.1. This geometry was a compromise between reducing its effect on the data reduction to a minimum, i.e. making the pointer as short as possible, and ensuring the specimen deflected freely beyond failure. The impactor is non-standard^[5]. The supports were the same as the ones used in the Nitrogen Gas Gun tests and are also non-standard. They were attached in this case to a massive lump of mild steel attached to the parallel

bars in the working section of the gun (see Fig. 6.11). Thus the supports can be regarded as being rigidly attached to the gun.

The specimens were made from DIN 17115 1.6753 chain steel and machined in line with the ASTM standard^[5]. Four specimens were milled per length of 36 mm diameter bar. Table 6.5 gives the composition of the steel. Table 6.6 gives the two heat treatments tested viz. 'as used' and 'toughnesstrough'. The fracture toughness of the steel is discussed in [9]. Note that the specimens were as machined and not fatigue pre-cracked.

6.2.5 Conduct of the Tests and Resultant Traces

All tests were carried out at room temperature, and this varied between 10° C and 20° C over the six month period of the tests. The specimen was placed centrally on the supports with the loading bar resting on the specimen. To perform a test the instrumentation was primed and the ball valve at the breech end of the gun opened manually. The pressure or vacuum in the reservoir and barrel were pre-selected to give the correct impact velocity. The impact bar travelled down the barrel, impacting the loading bar. A stress pulse travelled at 5000 ms⁻¹ down the loading bar eventually causing the pointer to accelerate and force apart the specimen. The loading bar was arrested and the working section covered to contain the resultant pieces.

A typical set of 'raw' traces is given in Fig. 6.12 and the settings of the instrumentation for this test are given in Table 6.4. The data reduction of these traces is described in Appendix C. One problem with the data analysis is that when the bar and instrumentation calibrations are taken into account there is a disparity between stress levels between gauges at S1 and S2 of approximately 8%. This is due to the pulse dissipating as it travels down the 2 m bar. Another set of gauges, S3, were placed halfway down the bar and the resultant trace is given in Fig. 6.12. The pulse for data reduction

was taken as starting at the upper level for gauges S2 and rising to gauges S1 - see Fig. 6.12.

The full data reduction techniques are given in Appendix C. From the 'raw' traces values of $P(t)$, $\delta(t)$, $P_{\max}$, P_f , t_f , CVNE and possibly K_{IC} or J_{IC} values could be obtained.

6.2.5.1 Results for DIN 17115 Chain Steel

Table 6.7 gives a summary of results for the two heat treatments. Note that the 'toughness trough' specimens are slightly over hard (see Table 6.6). The velocity of bar impact varied between 6 ms^{-1} and 10 ms^{-1} . The resultant $P(t)$ traces are given in Figs. 6.13 and 6.14. In the case of test A1, for example, the load oscillates due to the inertial oscillations of the specimens at a period of $41 \text{ } \mu\text{s}$ - this should be compared with Servers' value of τ of $29 \text{ } \mu\text{s}$ (see Table 3.1). The specimen ruptures before these inertial oscillations decay. Reducing the bar impact velocity increases the time to fracture until the specimen fails in a quasistatic manner, see for instance tests A3 and A4. In these two cases the particle velocity corresponding to the 'plateau' prior to failure is 7.2 ms^{-1} and 5.6 ms^{-1} respectively. These velocities correspond to forces of 31 kN approximately. Thus these velocities should be taken as the 'impact' speeds to relate to data from the Instrumented Charpy Test and it can be seen that only tests A4 and B4 satisfy the standard requirements of $3 \text{ ms}^{-1} < v_{\text{imp}} < 6 \text{ ms}^{-1}$ [5].

The average value of τ is slightly lower for the 'as used' specimen but this value is higher than Server's empirical value of τ . This is not surprising as it was shown in Chapter III that the period of vibration is dependent on the loading system. The ' 3τ ' criterion is valid for all the tests but Figs. 6.13 and 6.14 show that at the higher velocities the specimens rupture while they are still oscillating - there is a complex interaction between the impact

velocity and nature of loading in this case.

The response of both heat treatments shows extensive plasticity - there is no clear change from elastic to plastic behaviour. A detailed discussion of the response is given in the next paragraph. The average maximum force - at which the specimen is ruptured in a regime dominated by plasticity - is slightly higher in the 'toughness trough' case. This is expected due to the increase in yield point, i.e. hardness, for this heat treatment.

The data reduction method is complex and includes a number of assumptions and approximations viz.:

1. The calibrations of each load trace is the same;
2. The modification to the stress pulse as described in Section 6.2.5;
3. All data manipulation was completed by hand.

The maximum error in bar calibration (see Section 6.2.3) is 4% due to scatter. The error in instrumentation calibration, due to the use of an average, i.e. global, calibration is also 4%. The modification of the stress pulse, due to dissipative effects, is a subjective process (see Fig. 6.12). Possible errors are of the order of 5%. The interaction of these errors is difficult to gauge but the scatter in P_m in Table 6.7, for example, is of the order of the experimental error. The data reduction method would be improved by the use of a micro-computer. In this case digital information would be fed from the transients and manipulated by the computer in a consistent manner. The time for the analysis of results would be greatly reduced, the differing calibrations taken into account and the various errors inherent in correlating different 'raw data' traces quantified. For example, at the moment the location of $P(t = 0)$ point is subjective and the effect of the pointer is neglected (see Appendix C).

From the HPB analysis the particle velocity in the bars can also be derived giving deflections and work done by the applied force.

Table 6.7 gives the maximum deflection and total work done by the applied force prior to failure for the specimens that rupture after inertial oscillations have decayed, i.e. that display quasistatic response. Values of δ_m compare with the final deflection, estimated from putting the broken pieces together again and measuring the angle. Fig. 6.15 gives $\delta(t)$ for test A4 (see discussion in Appendix C). Note that $\delta(t)$ refers to section XY in Fig. 6.7. The total work done by the applied force goes into material damping, irreversible plastic processes, and final kinetic energy of the specimen pieces as well as into the creation of new surfaces. This total work can be compared with the Charpy Impact Energy for a pendulum machine. Fig. 6.16 gives the four values of CVN Energy from 'valid' tests and compares them to values obtained by the NCB^[9]. Agreement is close showing that the HPB method of loading gives values similar to the standard Charpy test. More data points are required before the effect of impact velocity can be gauged. The error due to the neglect of the effect of the pointer is discussed in Appendix C. Fig. 6.17 gives line drawings of the rupture surfaces, i.e. cleavage and shear lips. The CVN energy is similar for both heat treatments and so no large difference can be seen due to this effect. There is an impact velocity effect. At lower velocities the size of the shear lips become greater and lips are symmetrical showing some strain rate effect.

1 mm gauge length strain gauges were placed adjacent to the notch tip (see Fig. 6.7) in an effort to measure the time of crack initiation. Typical traces are given in Fig. 6.18 and are difficult to interpret due to extensive plasticity (see Section 6.2.5.2). The final drop does often correspond to specimen rupture but the method is not reliable. A small strain gauge was also put on the supports near the contact point (see Fig. 6.19) in order to measure contact force. The result for $v_1 = 9.41 \text{ ms}^{-1}$ and for an 'as used' specimen is given in Fig. 6.19.

The specimen never loses contact and oscillations slightly lag the applied force. Thus the HPB is a superior method of loading as compared to the Instrumented Charpy Test in which bouncing at the supports occurs^[10]. If a dynamic compliance is to be derived the boundary conditions for the HPB method of loading are more straightforward.

6.2.6 Further Data from HPB Tests

Can any more data be derived from the HPB tests described above? To answer this the behaviour of the specimen will be described in detail.

The specimen is impacted, via the loading bar, and oscillates at a period $\tau = 35 - 45 \mu s$ with an increasing mean load. These oscillations gradually decay. The loading bar and supports remain in contact at all times. At some point 'pop-in', i.e. crack initiation, occurs. This point cannot be easily identified in Figs. 6.13 and 6.14. When 'pop-in' occurs the load is shed onto the shear lips - thus the whole inertial response should change. 'Pop-in' should not be registered by a strain gauge, adjacent to the notch tip, on the surface of the specimen. Plastic flow occurs in the shear lips and eventually rupture starts at the notch. Rupture travels across the specimen at high speed as indicated by the comparison of strain gauge traces (Fig. 6.18) with the fall off in applied load (Figs. 6.13 and 6.14).

Any K_{Id} value has to apply to the moment of 'pop-in' and from the present tests this parameter cannot be obtained. The point of crack initiation is also required for J determination. Hence these tests cannot yield either fracture mechanics parameter. In order to achieve these, the specimen needs to be fatigue pre-cracked to increase plastic constraint and hence promote brittle fracture. Such a series of tests is described in Appendix D.

6.2.7 Conclusions

For standard CVN specimens the HPB method of loading is superior to the standard machines. It has been shown that HPB impact energies are directly comparable with standard values but that any further fracture information can only be derived if the specimens are pre-cracked. Thus the next stage in the research is to test PCVN specimens using the HPB approach (see Appendix D).

6.3 INSTRUMENTED IMPACT TESTING OF THE DNB GEOMETRY USING THE NITROGEN GAS GUN

6.3.1 Introduction

In the literature the most relevant impact test to the DNB is due to Duffy et al.^[14]. In this test the rising tensile pulse occurs for 35 - 40 μ s and fracture occurs typically in 28 μ s. A major disadvantage is that the specimen, i.e. the 40" long notched bar, is large and expensive. Any test, that is going to be proposed as an alternative to the Charpy test, has to use specimens within the 10 mm x 10 mm x 55 mm envelope due to the current nuclear testing program in the U.S.A. As a step towards this, a fracture toughness specimen was developed that could quickly and easily be incorporated into existing laboratory equipment, and more specifically the Nitrogen Gas Gun. The material used for these exploratory tests was En24 - such a material is widely used, well documented^[13] and displays a wide variation in material properties, as a result of different heat treatments. The material has the same specification as AISI 4340.

6.3.2 The Loading System and Specimen

The adaptation of the Nitrogen Gas Gun is given in Fig. 6.20. The apparatus was designed for ease and simplicity in manufacture. The loading tup used in the Charpy tests (see Fig. 6.2) was replaced by a 15.5 mm diameter bar with an axial hole 110 mm deep for the

specimen. The specimen was machined from 16 mm diameter bar. Thus when the 0.75 m bar impacts the loading bar, the compressive wave travels up the 0.4 m bar and displaces interface A to the right. Also the compression wave is reflected as a tensile wave at face B, thus the resultant stress pulse is made up of these two components. The stress pulse travels into the notched specimen, which is in the form of a 5 mm x 5 mm bar, and is incident on the notched section. The pulse travels past, loading the notched section, reflects as tension at end C, travels back up the specimen and unloads the notched section. Thus the period of loading at the notches is dependent on the length NC. If NC = 80 mm the loading time is 32 μ s. The dimension was selected as a compromise between loading time and ease of manufacture and heat treatment of the specimen. The specimen was glued onto the loading bar using a fast setting acrylic glue. The notches were the same as 'V' notches used in Charpy tests. No fatigue pre-cracking was used.

6.3.3 Instrumentation and Conduct of the Test

In such a test the applied loading and time of crack initiation were required. These were to be achieved by using the trace from a 1 mm foil gauge mounted upstream of the notched section (see Fig. 6.20). Of course such a gauge would also register diffraction and reflection effects from the notch tips and crack initiation would only be measured for brittle fracture. Leads from the gauge passed out through the back of the specimen. Due to the fast times expected, a 20 MHz transient recorder was required. The signal from the gauge, incorporated into the bridge shown in Fig. 5.12, was amplified and stored on the transient. The transient was triggered using the circuit described in Section 6.2.3 with signals from 6 mm strain gauges (see Fig. 6.20) as input.

To perform a test the specimen was mounted, instrumentation primed and the gas gun fired manually. The specimen and bars were arrested

using a copper buffer mounted on a hydraulic ram.

6.3.4 Results and Discussion

The composition and heat treatment of En24 are given in Tables 6.9 and 6.10. Results are given in Table 6.11. At no point did any specimen fracture, i.e. the notched section unloaded before crack initiation could occur. This is discussed below. Various parameters of the test were varied in an effort to obtain fracture.

6.3.4.1 The Effect of Impact Velocity (Tests C1, C2)(Fig. 6.21)

Test C1 gives a linearly rising pulse to maximum load showing that the constituent pulses from interface A and face B combine to give ramp response. Increasing the impact velocity increases the rate of rise of the initial pulse. A 'knee' occurs in trace C2 at a lower value than $\sigma_{\max}$ in trace C1. The strain at this 'knee' is 0.4%, this is within the strain limit for the gauge. Of course the stress gradients within the gauge are high, but the 'knee' could be attributed to yielding. Any dynamic compliance would embrace elastic behaviour only. Note the rise time for the elastic response (C1) is 28 μs - slightly shorter than the 32 μs predicted. The probable reason that the specimen did not fracture is that the constraint at the notch tip is not great enough - a circularly grooved bar with a fatigue pre-crack might well have fractured. In the test one is depending on brittle fracture, as any plastic rupture takes much longer than the 30 μs allowed.

6.3.4.2 The Effect of Heat Treatment (Tests C2, C3)(Fig. 6.22)

'As quenched' material is not as homogeneous as tempered material and so should display more brittle behaviour. The CVN Energy is reasonably linear between the two heat treatments^[13], but the yield point increases for the 'as quenched' so promoting brittle fracture for a given constraint and temperature. The responses in tests C2 and C3 are similar as material properties are similar, yielding taking place

slightly later for the 'as quenched' condition, as expected. The situation has not been improved enough to give rise to fracture.

6.3.4.3 The Effect of Notch Depth (Tests C3, C4)(Fig. 6.23)

The next step was to increase the notch depth so as to increase $K_I(r)$. Again fracture did not occur. It must be concluded that the effect of the blunt notch, delaying crack initiation, dominates the increase in $K_I(t)$ due to the deeper notch.

The difference in $\sigma_{\max}$ is presumably due to diffraction effects and plastic fields, both of which increase in strength for the deeper notch. It is difficult to say whether the strain indicated by the gauge is an accurate measure of the nominal stress. It was felt that the notch depth could not be increased any further, and so the conclusion was that for this, very brittle, material the test could not be achieved. The conclusions from this are given in Section 6.3.5.

6.3.4.4 The Effect of Specimen Geometry (Tests C1, C5)(Fig. 6.24)

To illustrate the effect of changing the distance between interface A and face B, test C5 was conducted. As expected, the constituent pulses separate out, reducing the intensity of loading. Thus the type of specimen used in tests C1 - C4 gives a better result. Reducing the distance between interface A and face B would make the loading head more compliant, increasing rise time.

6.3.5 Conclusions

With the current apparatus and specimen, brittle fracture cannot be achieved for steels of engineering interest. Although the given loading has been shown to give failure in similar specimens^[14], the lack of constraint in the current tests has meant that fracture has

not occurred. The downstream dimension of the specimen, NC in Fig. 6.20, could be extended to increase time to unloading but the specimen would then become impractically large.

A major limitation of the stress wave loading approach is that the loading times are so short. Any plasticity would greatly increase the loading time. An obvious step would be to dissociate the specimen from the apparatus, and have a separate loading head and inertia bar (see Fig. 6.25). The specimen would be mounted in the apparatus using screw threads, or similar connections. The problem with such a system would be its stress wave behaviour; an elastodynamic compliance would be complex given the complex load transmission paths in and out of the specimen. Thus in using this apparatus one would be concentrating on inertia loading, and the load would be monitored by a strain gauge mounted on the inertia bar, for example. In developing such an apparatus the best approach would be to build a set up and investigate its behaviour, as it is difficult to quantify its stress wave behaviour. The loading rates, giving rise to failure, would be lower than those for stress wave loading but would be, arguably, of greater technological interest.

6.4 REFERENCES, TABLES, FIGURES

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Table 6.1 Composition of Silver and DIN17115 Steels

	C	Mn	Ni	Cr	Mo	S	P	Si
Silver Steel [†]	1.1-1.2	0.3-0.4	-	0.4-0.5	-	0.045	0.045	0.1-0.25
DIN 17115	0.22	1.50	1.05	0.33	0.43	0.014	0.017	0.23

[†] Specimen material not analysed, data from handbook

Table 6.2 Heat Treatments for Specimens tested on the Nitrogen Gas Gun

Specimen A	Silver Steel	Heat in flame to 1000°C for 1 min Quench in Oil $R_c = 60$
Specimen B	Silver Steel	Heat in oven to 805°C for 1 hour Quench in Oil $R_c = 42$
Specimen C	DIN 17115	Heat in oven to 900°C for 1 hour Quench in Water Temper @ 300°C for 3 hours $R_c = 43$

Table 6.3 Results for Nitrogen Gas Gun Tests

Specimen	Reservoir psi	σ MPa	ϵ $\times 10^{-3}$	v_2 ms^{-1}	v_1 ms^{-1}	v_3 ms^{-1}	τ_f μs	P_m kN	P_f kN	τ μs	t_f/τ
A	250	371	1.51	7.80	15.61	-	6	20.4	20.4	44	0.1
B	250	308	1.47	7.59	15.20	-	26	21.1	7.55	44	0.6
C	250	312	1.49	7.70	15.40	12	140	28.6	24.9	44	3.2

Table 6.4 Instrumentation settings for trace given in figure 6.12

Amplifier	Gain	$\times 100$
Transient	Full Scale Deflection	IV
	Sweep	1000 μs
Calibration	ΔV Input	2mV
Plotter	X	0.5V/cm
	Y	Sweep 100 mm/min

Table 6.5 Composition of DIN 17115 chain steel

C	Si	P	Mn	S	Cr	Ni	Mo
0.22	0.23	0.017	1.50	0.0014	0.33	1.05	0.43

Table 6.6 Heat treatment of DIN 17115 chain steel

Austenitize @ 900°C for at least 1 hour
Water quench
Temper @ T for 3 hours
Air cool

where T = 425°C for 'as used' (HV30 should be 380 [9])
T = 300°C for 'toughness trough' (HV30 should be 430-440 [9])

Table 6.7 Results for DIN 17115 chain steel (see over)

Table 6.8 Properties for DIN 17115 chain steel [9]

	HV30	UTS (MPa)	$\sigma_{0.5}$ (MPa)	$K_{IC}^{\dagger}$ (MPa $\sqrt{m}$)	$K_{IQ}^{\dagger}$ (MPa $\sqrt{m}$)
'As used'	384	1190	1110	-	115
'Toughness Trough'	443	1390	1280	70	-

($\dagger$ for 22x22 mm specimen).

Table 6.9 Composition of En 24 steel [13]

C	Si	P	Mn	S	Cr	Ni	Mo
					0.90-	1.30±	0.20-
0.35-0.45	0.10-0.35	0.050 max	0.45-0.75	0.050 max	1.40	1.80	0.35

Table 6.10 Heat Treatment of En24

Heat @ 835° for 1 hour
Quench in Oil
Temper @ T for 1 hour
Air Cool
where T=400° for brittle properties

Table 6.7 Results for DIN 17115 Chain Steel

Test	HV 30	σ MPa	ϵ $\times 10^{-3}$	v_a (ms^{-1})	v_3 (ms^{-1})	v_1 (ms^{-1})	t_f (at $P_m/2$) (μs)	P_f (kN)	P_m (kN)	γ (μs)	t_f/γ	δ_m (mm)	CVNE (J)
A1	449	245	1.16	6.03	-	9.41	120	29.2	31.4	41	2.9	-	-
A2	441	216	1.03	5.32	-	8.30	171	28.8	30.4	39	4.4	-	-
A3	444	206	0.98	5.07	7.2	7.91	237	31.2	32.4	39	6.1	2.02	46.7
A4	439	169	0.80	4.16	5.6	6.49	266	30.0	31.2	42	6.3	1.72	37.5
B1	377	248	1.18	6.11	-	9.53	141	32.6	32.6	38	3.7	-	-
B2	385	206	0.98	5.07	-	7.91	174	30.8	30.8	35	5.0	-	-
B3	389	202	0.96	4.97	7.4	7.75	261	30.0	30.0	33	7.9	2.07	48.3
B4	385	174	0.83	4.28	6.1	6.68	312	29.6	29.6	35	8.9	2.03	47.2

Toughness
Trough

As
Used

Table 6.11

Test	Material Condition	σ_y (MPa)	σ_{max} (MPa)	From [13]		Hardness R_c	ϵ_y (%)	t_y (μs)	t_m (μs)	a (mm)	K_{Ic} [15] (MPa $\sqrt{m}$)	σ_{nom} (MPa)	t_e (μs)
				$\sigma_{0.2}$ (MPa)	σ_{UTS} (MPa)								
C1	QT ⁺	1280	1280	1389	1698	45	0.56	28	28	1	-	-	-
C2	QT	886	1182	1389	1698	45	0.39	13	32	1	-	-	-
C3	Q*	985	1162	1466	1853	55	0.43	13	23	1	53	822	31
C4	Q	1024	1261	1466	1853	55	0.45	14	30	1.5	53	671	35
C5	Q	-	837	1466	1853	55	-	-	34	-	-	-	-

+QT - Quench and Temper
*Q - Quench only.

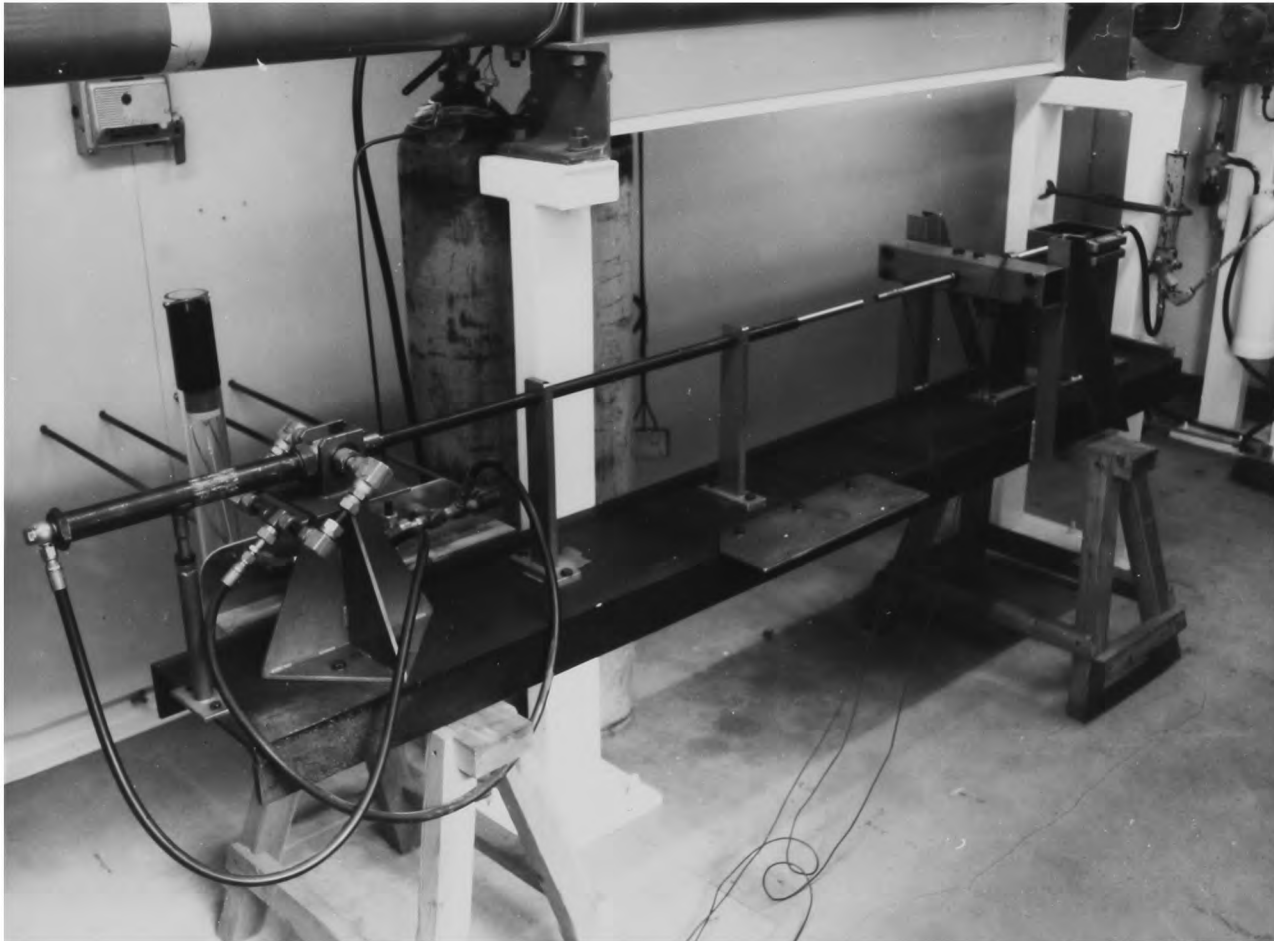


Fig. 6.1 General view of Nitrogen Gas Gun set up for Charpy Testing.

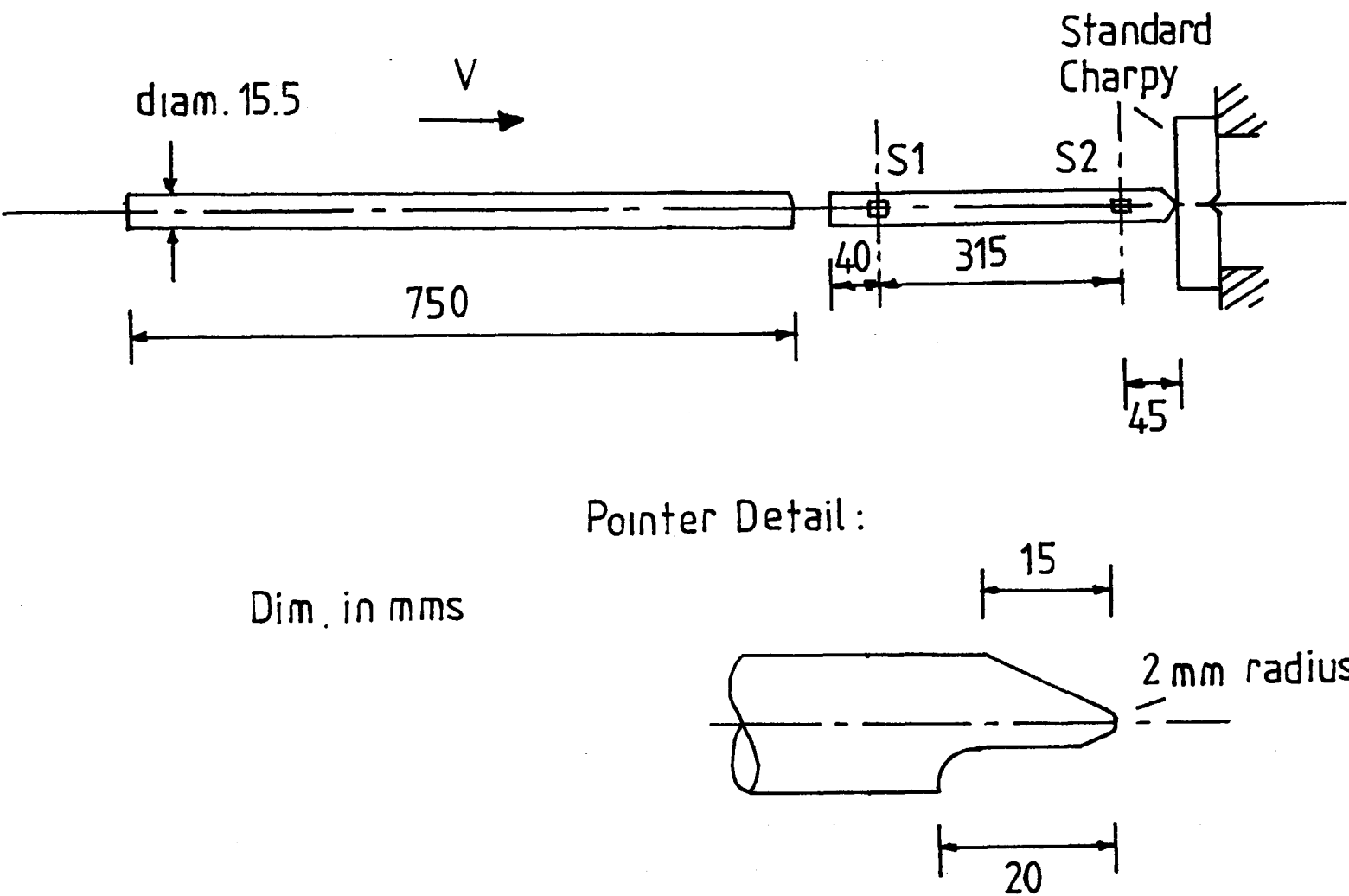


Fig. 6.2 Schematic of Nitrogen Gas Gun set up for Charpy Testing.

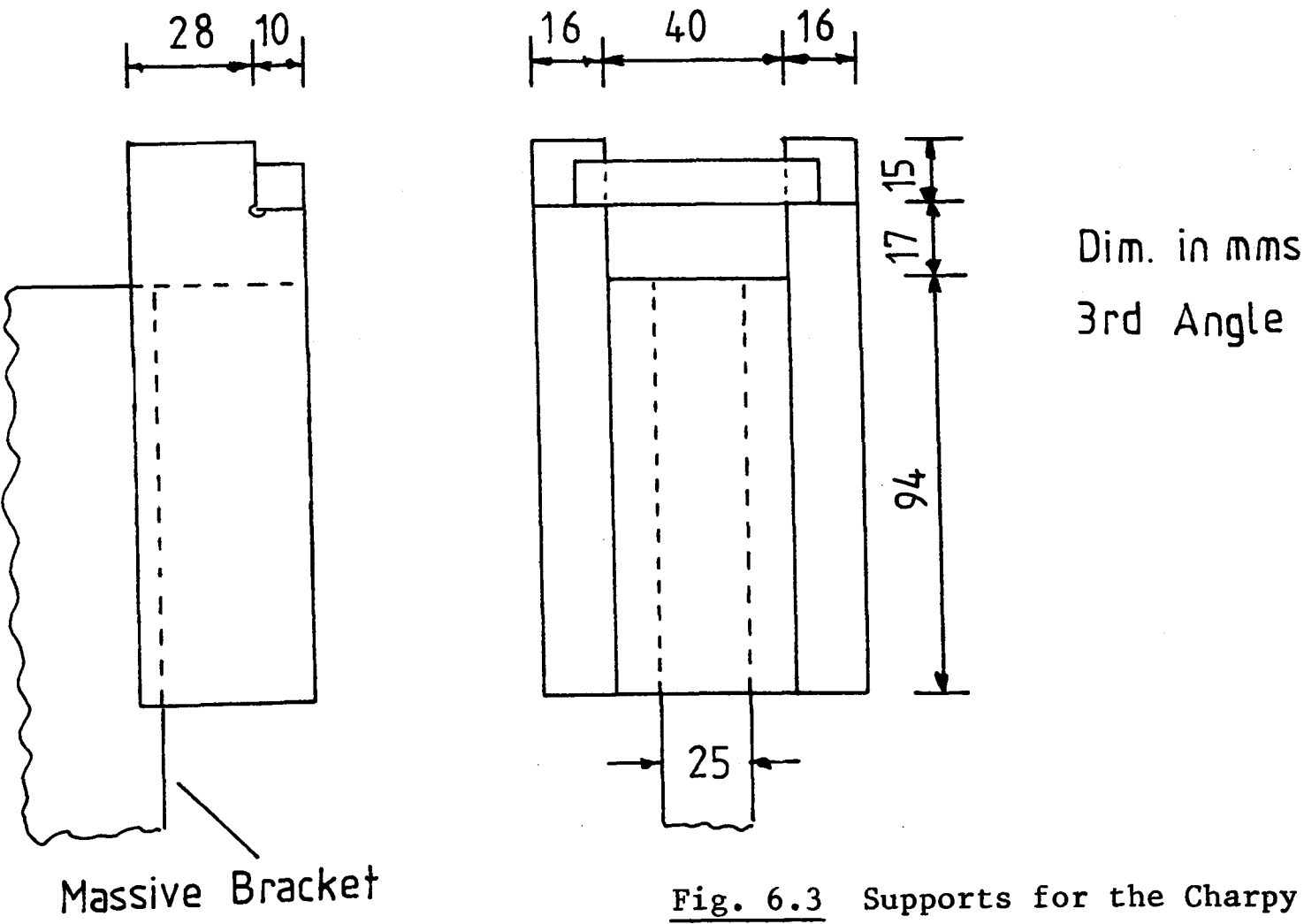


Fig. 6.3 Supports for the Charpy Test.

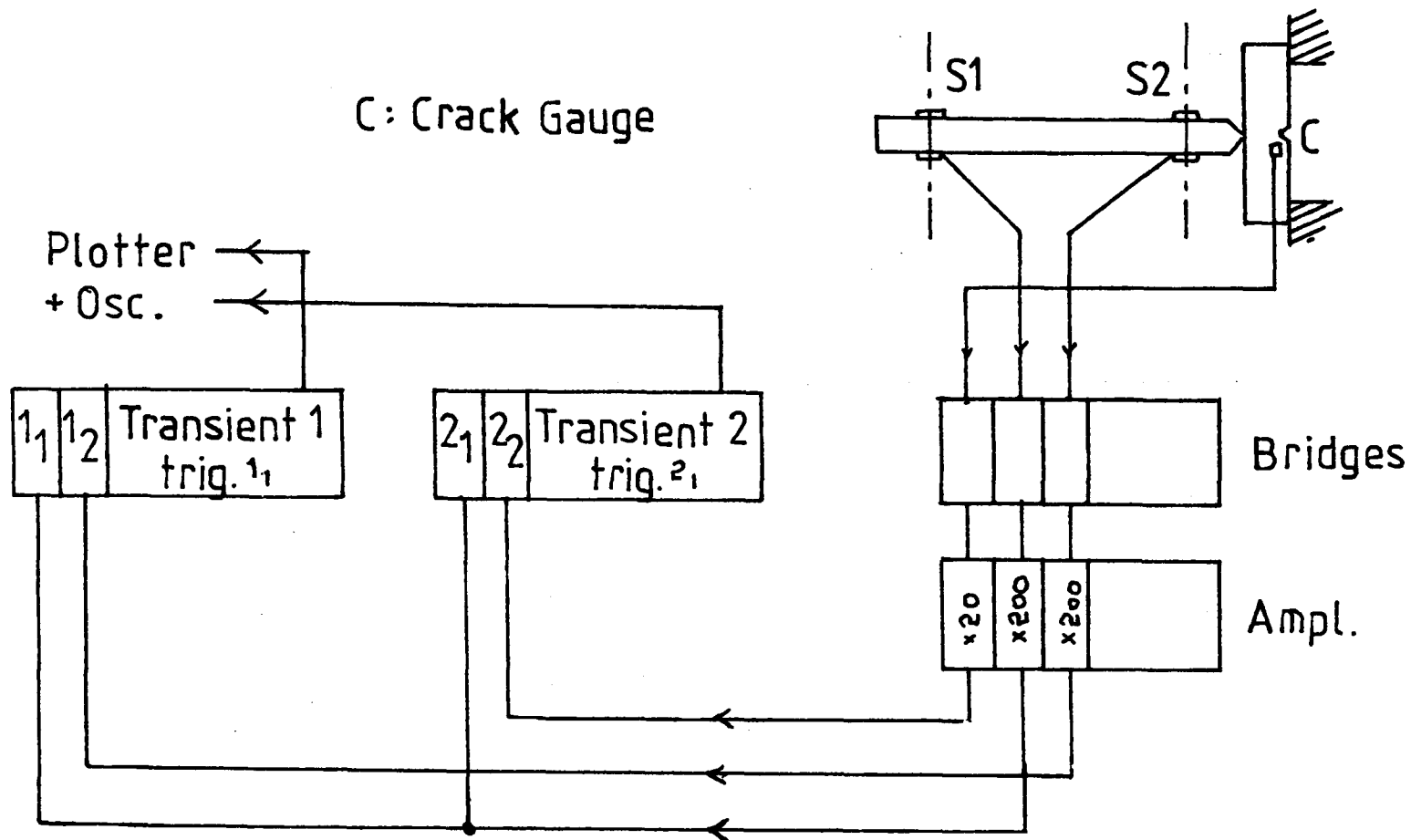


Fig. 6.4 Instrumentation for the Nitrogen Gas Gun Charpy Tests.

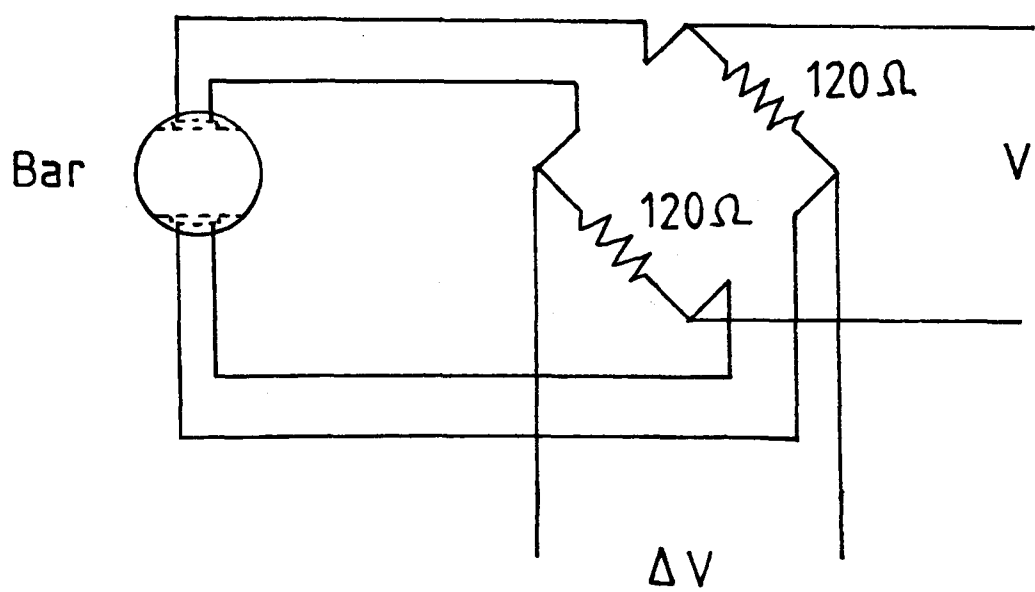


Fig. 6.5 Strain gauge system for loading bar.

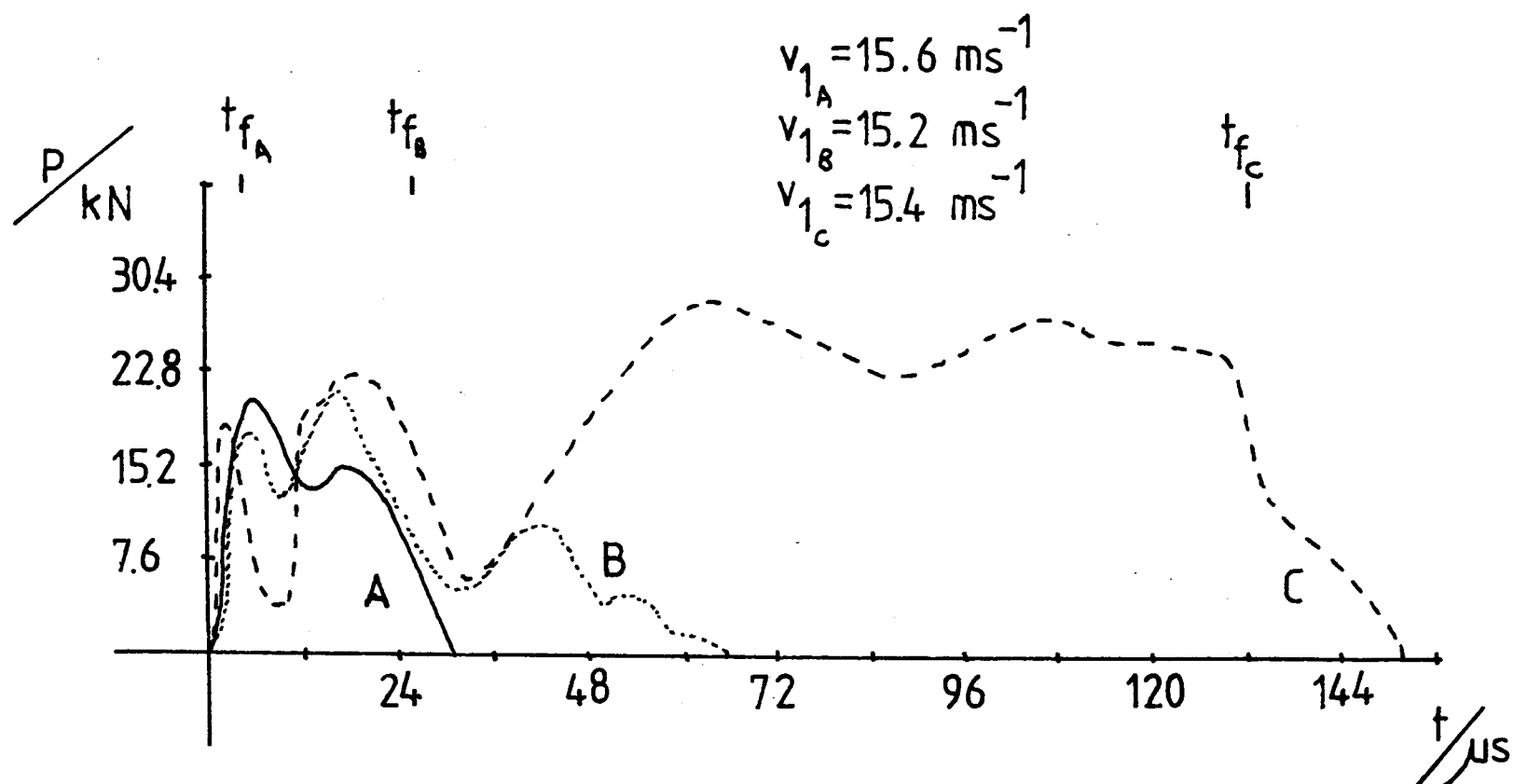


Fig. 6.6 $P(t)$ curves for Nitrogen Gas Gun Charpy Tests.

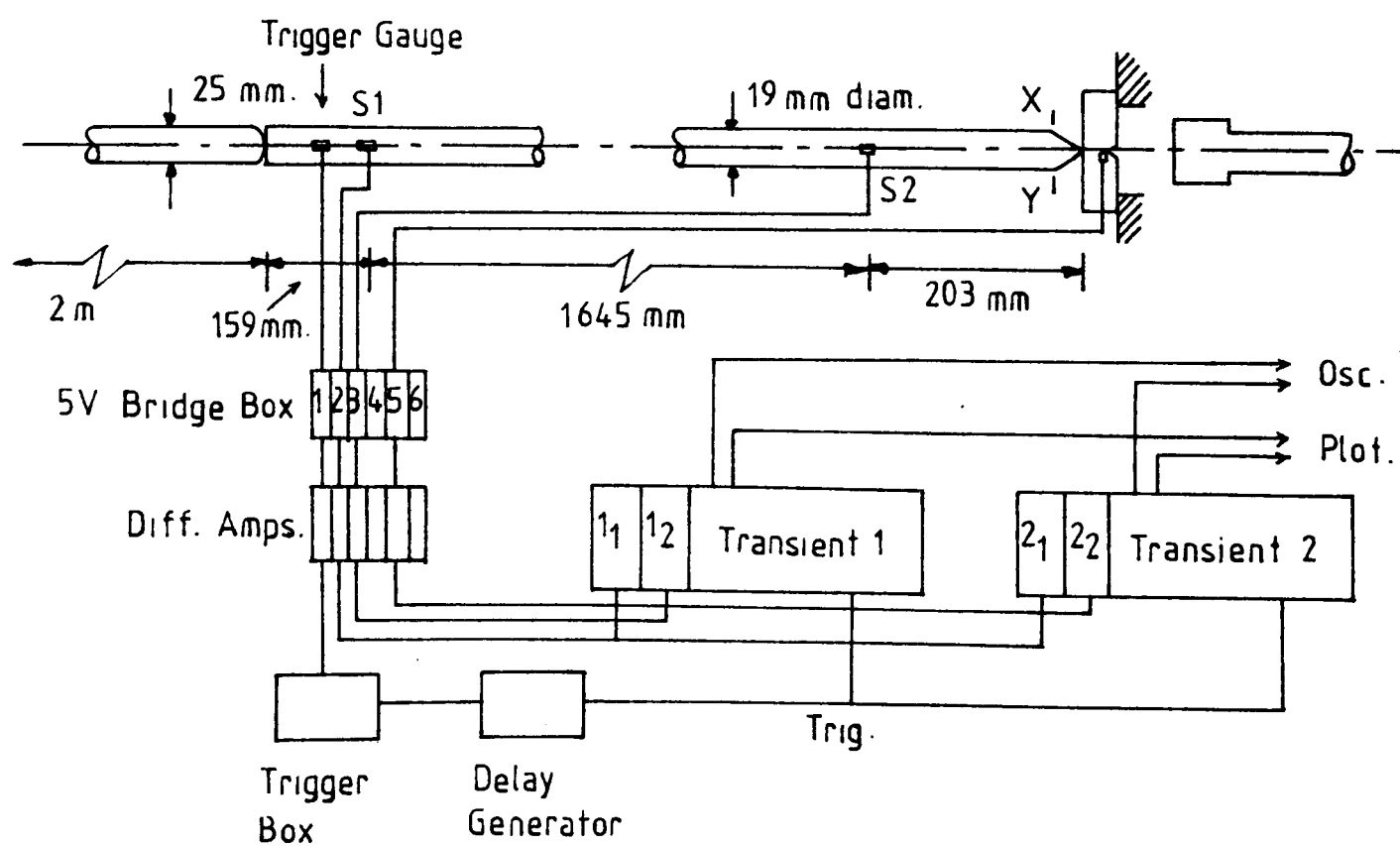


Fig. 6.7 Schematic and Instrumentation for the Air Gas Gun.

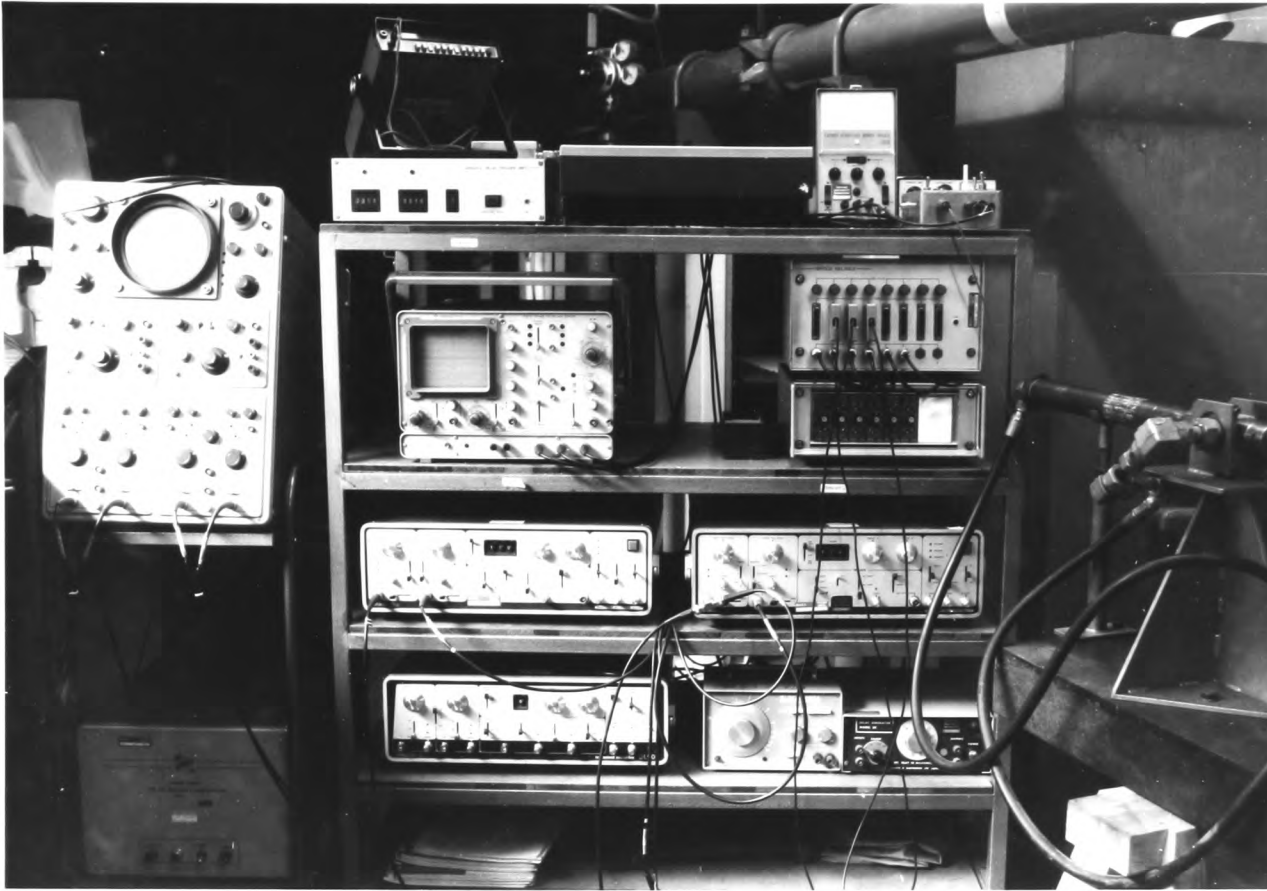


Fig. 6.8 General view of instrumentation.

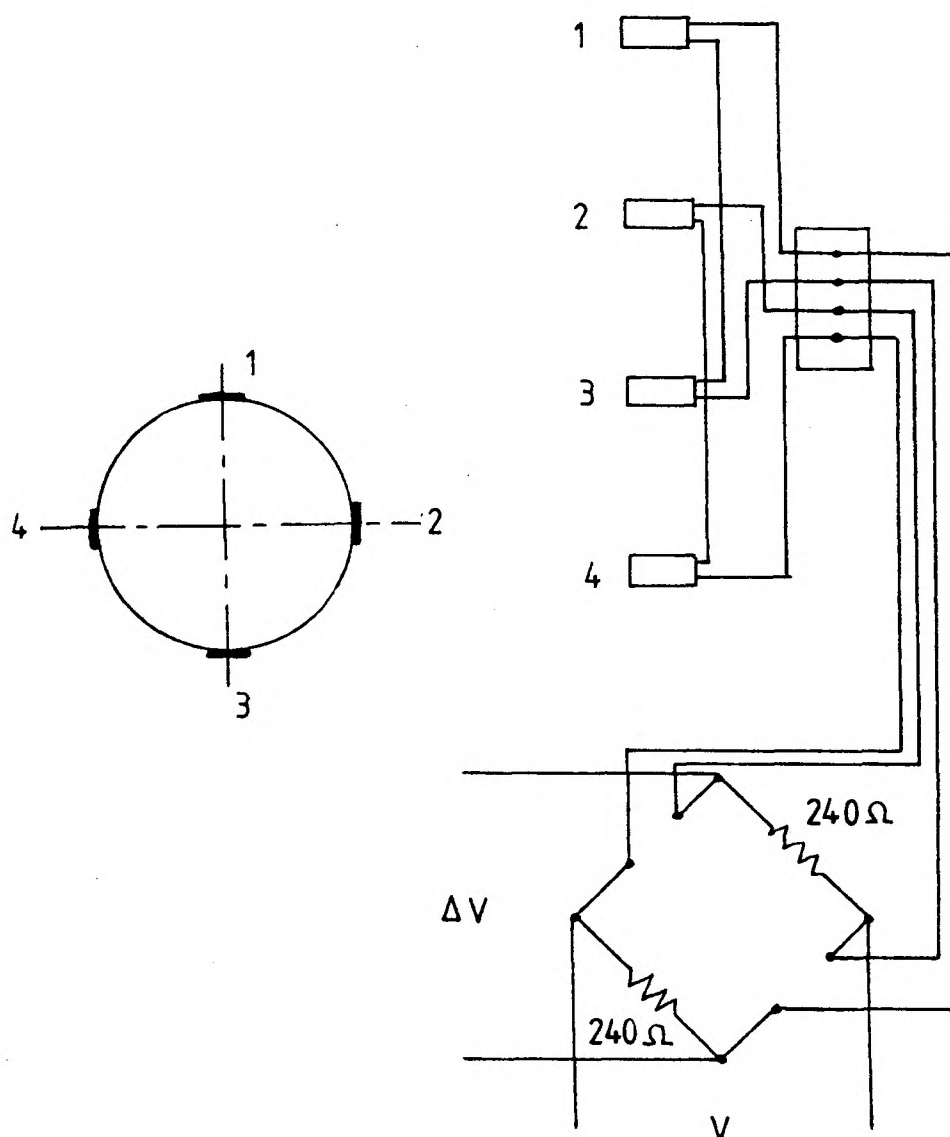


Fig. 6.9 Strain gauge system for loading bar.

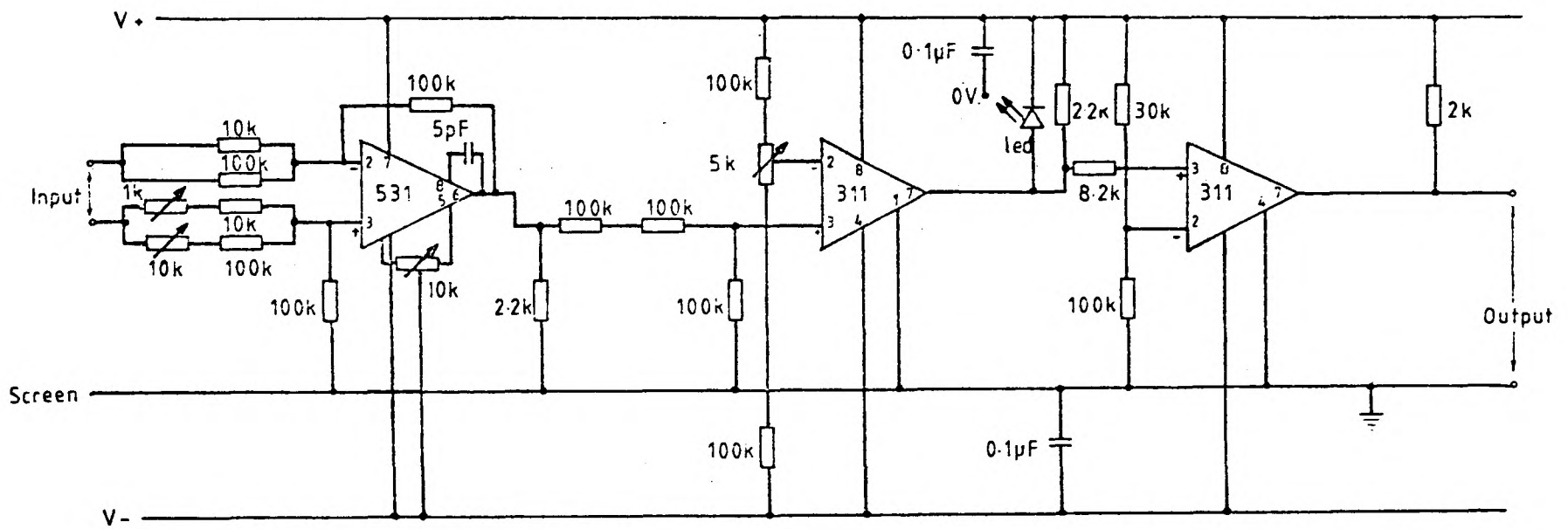


Fig. 6.10 Electrical circuit for trigger box.

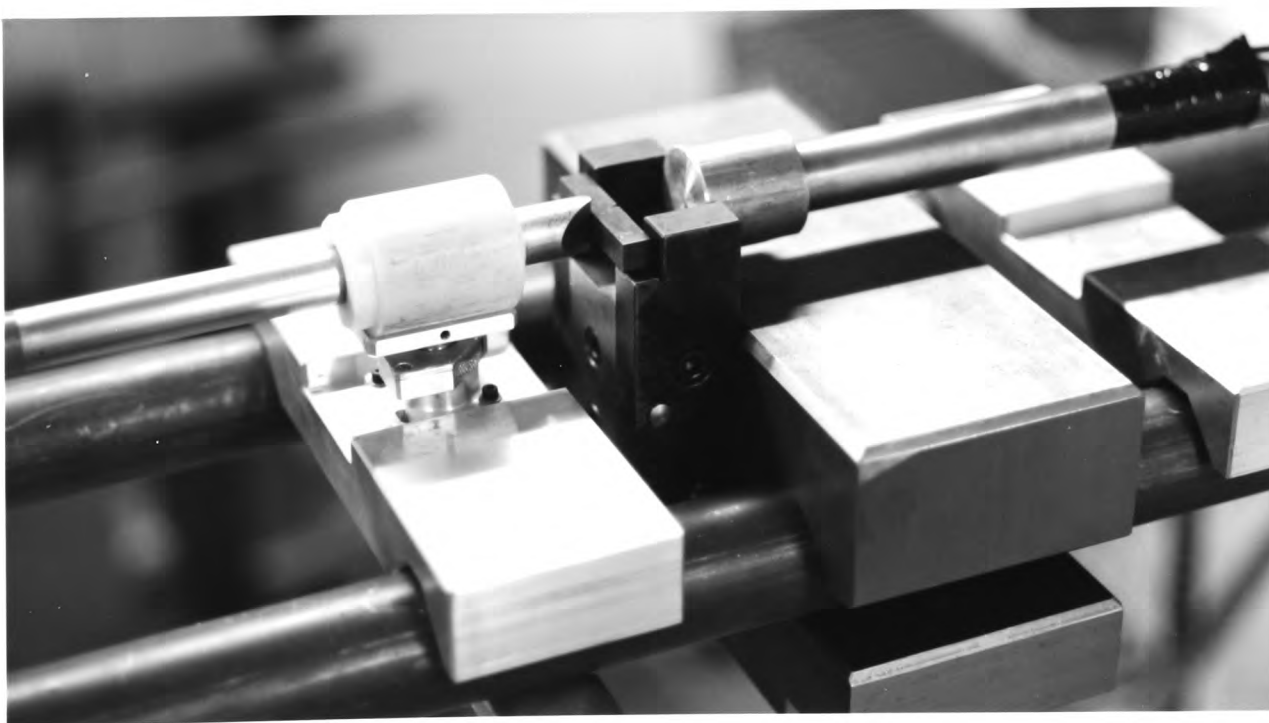


Fig. 6.11 General view of working section of Air Gun.

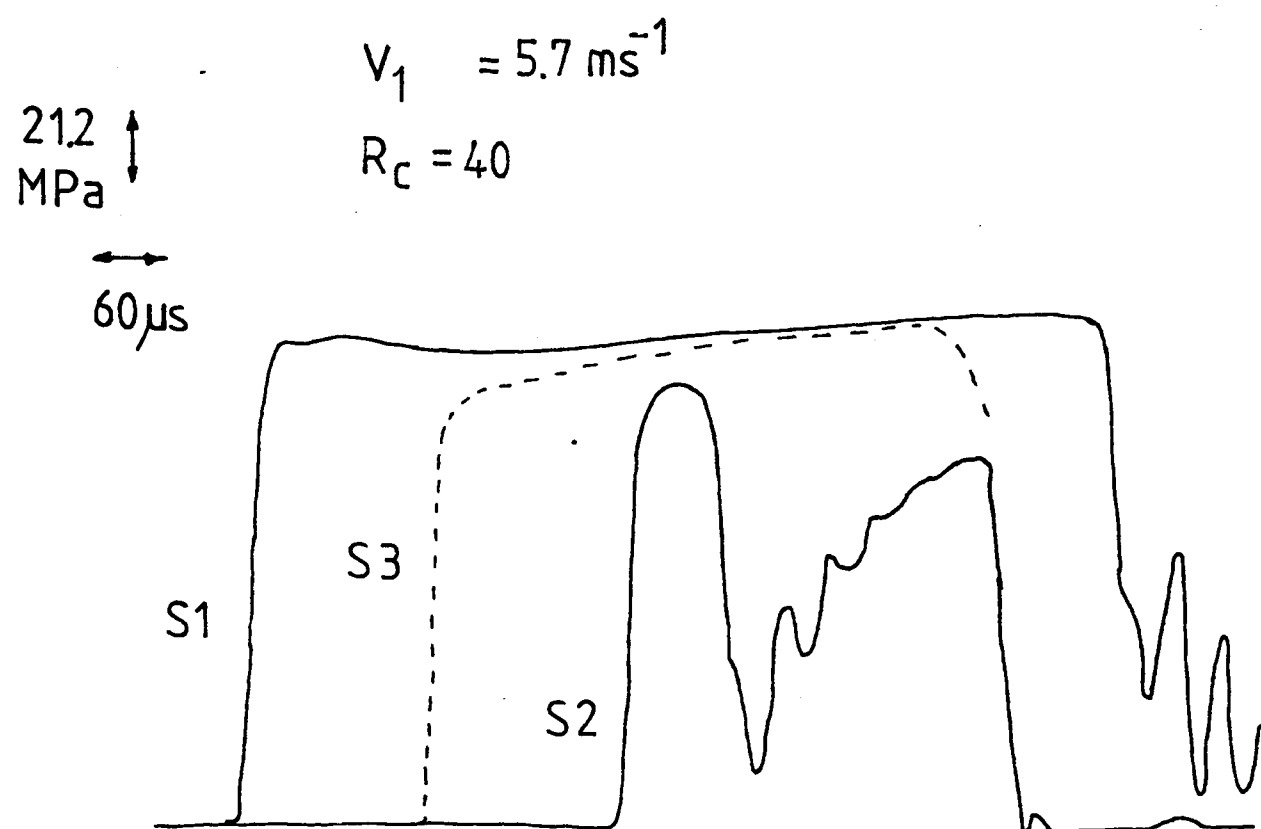


Fig. 6.12 'Raw Data' traces.

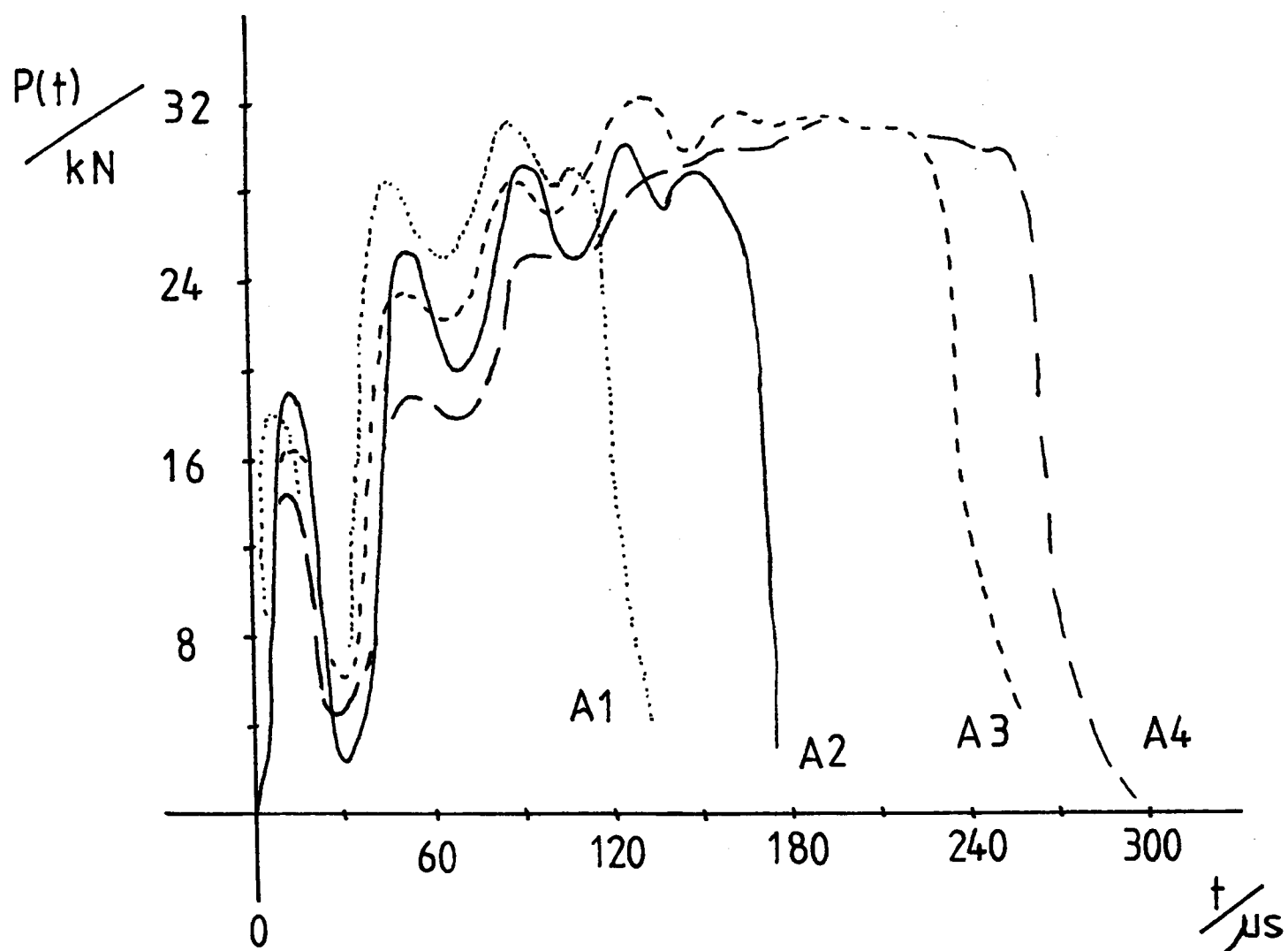


Fig. 6.13 $P(t)$ curves for 'Toughness Trough' specimens.

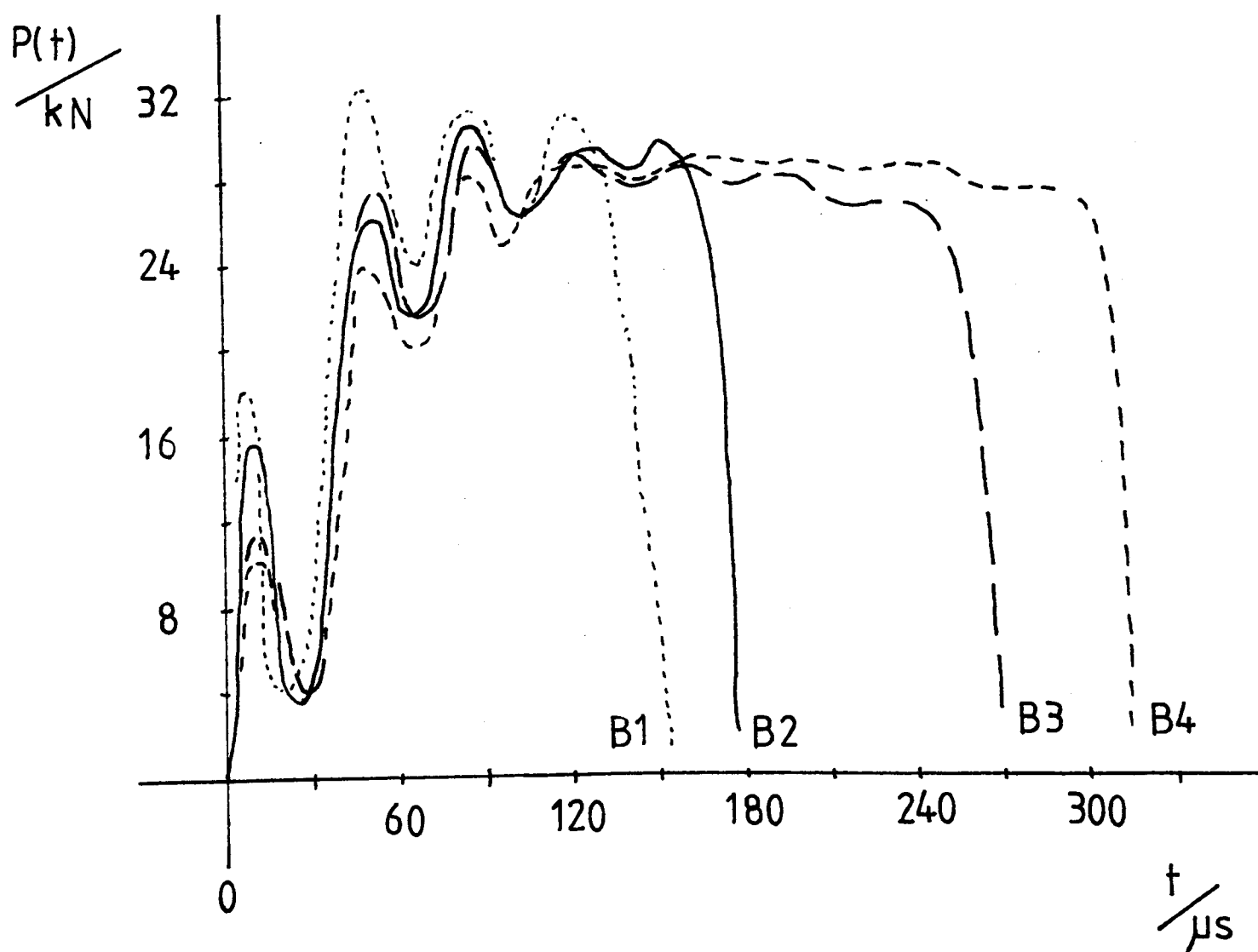


Fig. 6.14 $P(t)$ curves for 'As Used' specimens.

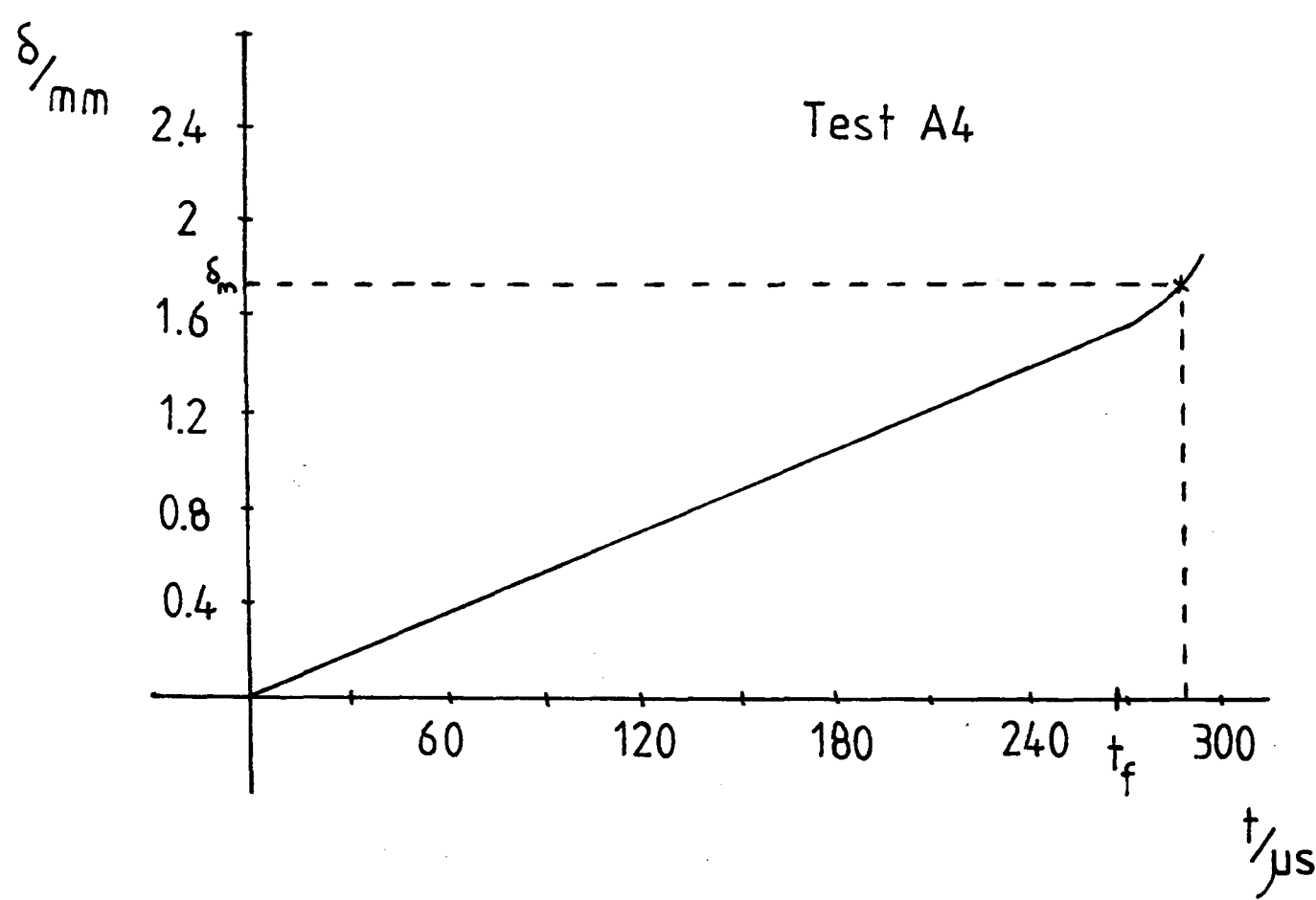


Fig. 6.15 $\delta(t)$ curve for Test A.4.

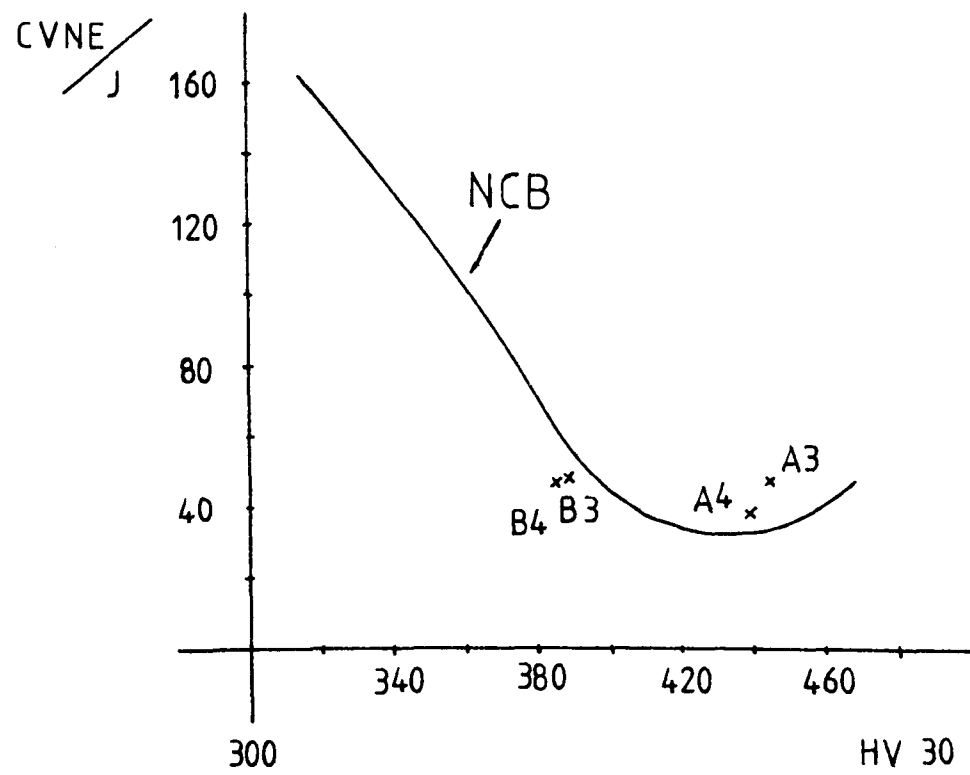


Fig. 6.16 Comparison of CVNE data with NCB values.

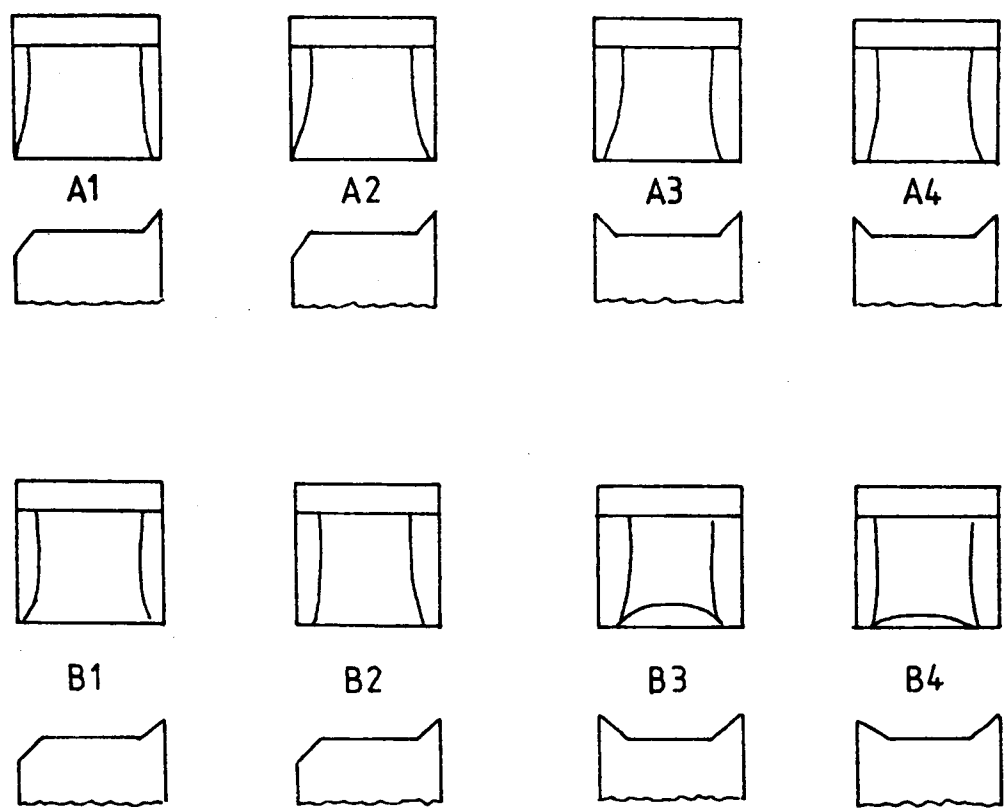


Fig. 6.17 Line drawings of fracture surfaces.

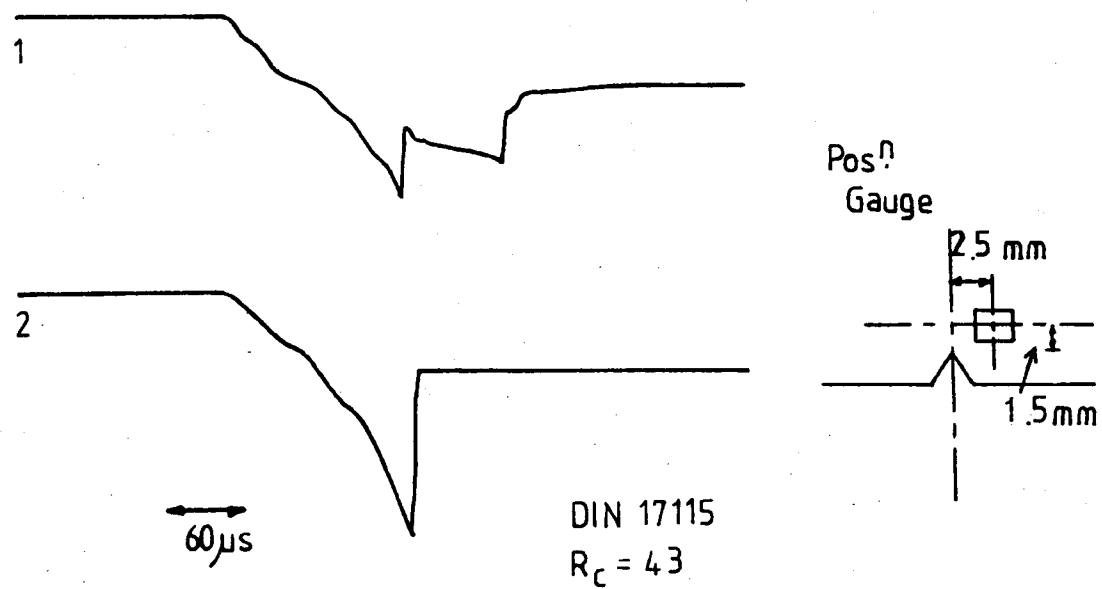
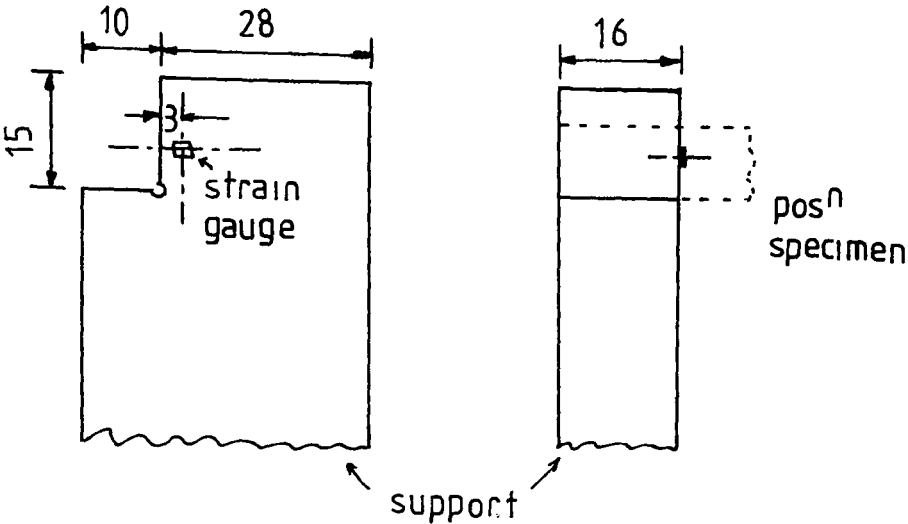
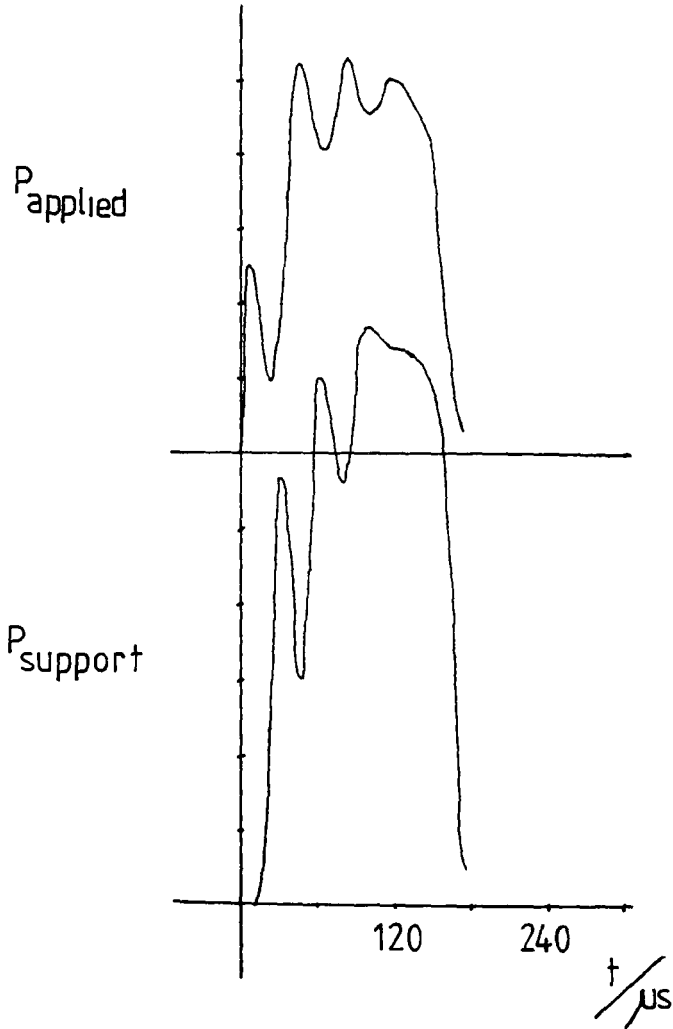


Fig. 6.18 Typical crack initiation gauge traces.

Fig. 6.19 Comparison of loading bar force with support reaction for a typical test.



first angle proj.
dimensions in mms



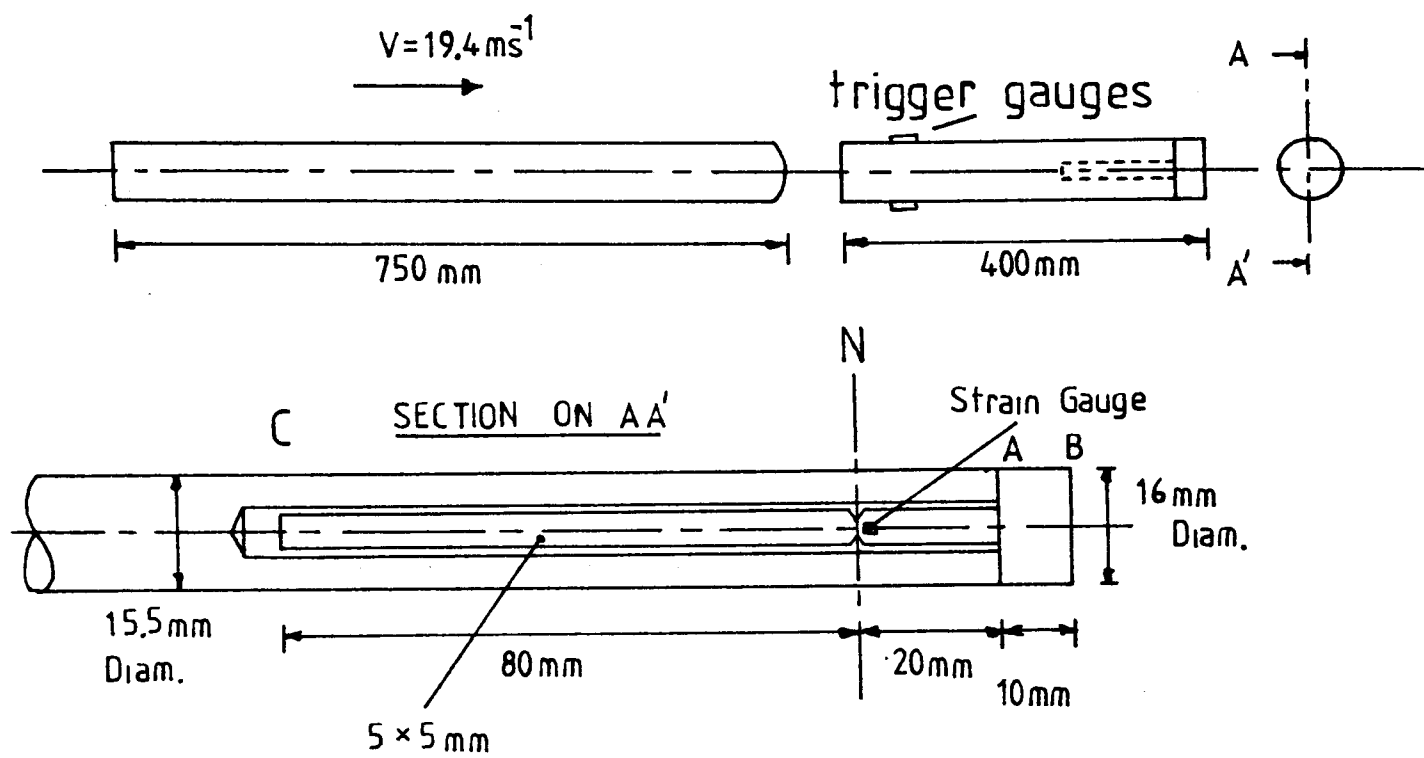


Fig. 6.20 DNB Instrumented Impact Test - general arrangement.

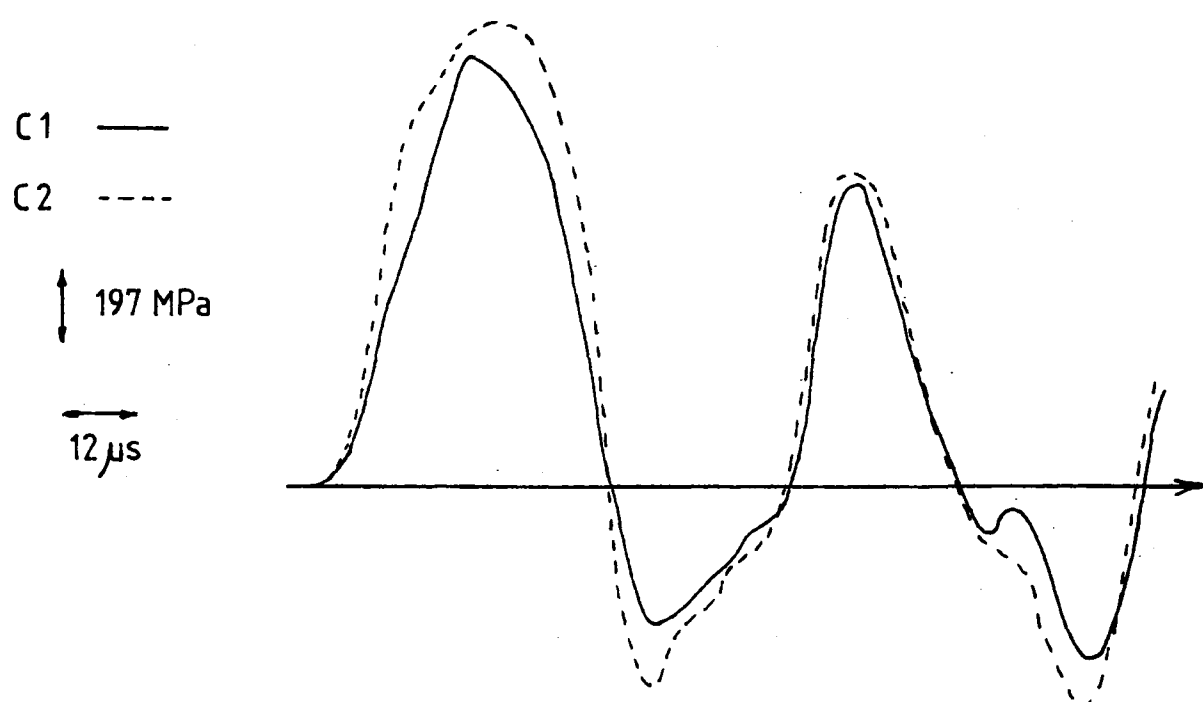


Fig. 6.21 The effect of impact velocity in DNB test.

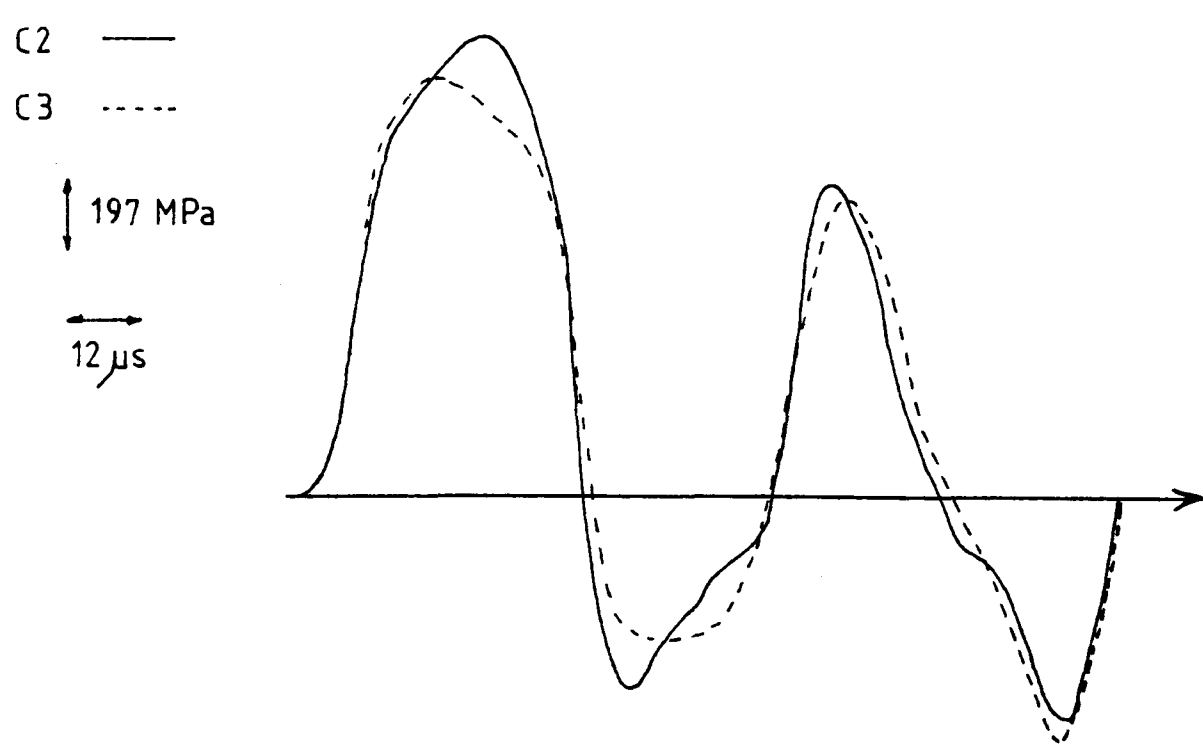


Fig. 6.22 The effect of material properties in DNB test.

Fig. 6.23 The effect of notch depth in DNB test.

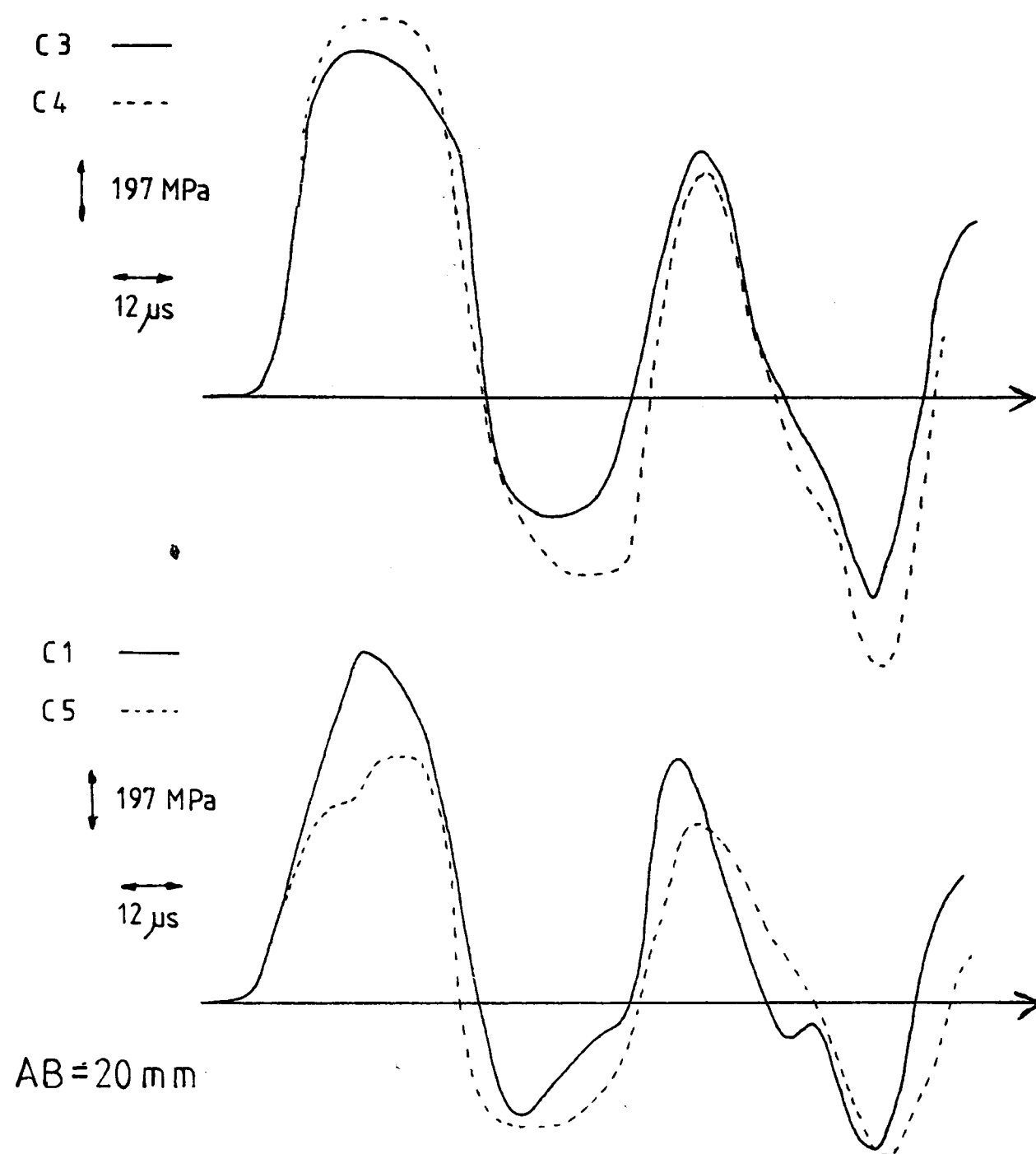


Fig. 6.24 The effect of specimen geometry in DNB test.

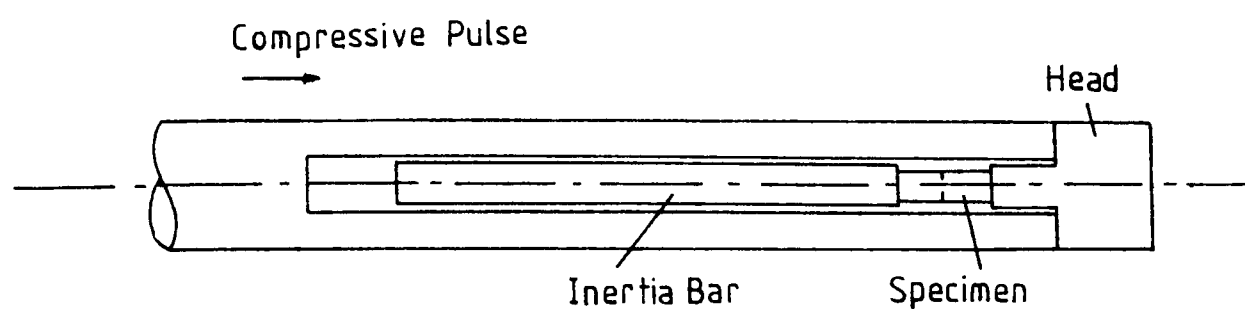


Fig. 6.25 DNB test adapted for inertial loading.

7. Conclusions and Recommendations for Further Work

The Instrumented Charpy Test is an improvement on empirical K_{IC} v CVNE correlations, but resulting fracture toughness values are still only approximate given the indefinables associated with crack initiation at maximum load and the use of a quasistatic *calibration*. The Hopkinson Pressure Bar method of loading is an improvement over the Standard Instrumented Charpy Test in that the applied load is specified more exactly, the mechanics of the apparatus is simpler and the specimen does not bounce prior to failure. It has been shown that CVNE for 'as machined' specimens derived using the HPB compare with values derived from the standard Instrumented Charpy Test.

More data is required from the Hopkinson Pressure Bar tests using PCVN specimens. *Preliminary tests are described in Appendix D* The current apparatus, viz Air Gas Gun and Nitrogen Gas Gun, are difficult to operate at the required low velocities and energies, and it is suggested that a ballistic pendulum be used. In the latter the existing one metre long impact bar would be suspended from the ceiling and dropped under gravity, analogous to a gravity pendulum. In this way the impact velocity could be varied in steps of 0.2 ms^{-1} and the rate effect on specimen behaviour evaluated in detail. The data reduction could be automated, using a microcomputer, so that a large number of tests would be possible and the data reduction could be achieved in a consistent manner. Although the mechanics of the HPB is reasonably straightforward, the derivation of results is a complex process subject to a number of approximations and assumptions. Thus given these extensive and accurate results, the validity of the proposed standard on Instrumented Charpy Testing could be investigated in more detail - the HPB tests do not get away from the assumptions of crack initiation at maximum load or the use of a quasistatic *calibration* for

the derivation of K_{Id} values. Hence the HPB tests are still restrictive, and still give approximate results.

It has been shown that the dynamic behaviour of the Charpy geometry is complex viz the impactor - specimen - support interaction and the specimen bouncing prior to failure. Dynamic Photoelastic studies have shown that the growth in stress intensity factor is dependent on the bouncing of the impactor. It is possible to derive an elastodynamic *calibration* for the specimen, using the experimental method of caustics for example, but the specimen has to be made oversize and the associated techniques are complex. Such tests are scientific in nature and diverge from the technological requirements of small specimens and straightforward testing arrangements. Such tests are required, though, for the accurate determination of K_{Id} .

Thus the scientific determination of K_{Id} should be regarded as distinct from the technological requirement of the derivation of fracture toughness values - the latter has been addressed in this thesis.

A possible alternative to the scientific study of the 3 point bend specimen is the testing of a notched geometry with a more straightforward dynamic behaviour. It has been shown that the Izod, Stender Cantilever and Double Notched Bar geometries display more simple behaviour, the latter being the simplest. In the case of the Double Notched Bar the growth in $K_I(\dot{t})$ follows closely the applied stress. Although the dynamic *calibration* of the Double Notched Bar is straightforward, the testing of such a geometry is more difficult. Here again there is the scientific and technological approach. In the former the specimen is large and expensive viz the Circularly Grooved Bar tested at Brown University. From initial tests described in this thesis, using a small specimen under technological conditions, it is proposed that a small DNB be inserted

between the loading head and an inertia bar. In this way the loading pulse is long enough to cause material rupture. By doing this the applied load has to be inertial rather than stress wave in nature. Using the above methodology accurate K_{Id} values can be derived in a simpler fashion compared with the Charpy test.

Often the Charpy specimen displays an elasto-plastic behaviour. The embrittling effect of strain rate can be used by impacting the specimen faster, and using a dynamic calibration, or by using a geometry with a faster loading characteristic, viz the Double Notched Bar. The derivation of elasto-plastic parameters is more accurate in the case of the HPB Instrumented Charpy Tests in that deflection and force measurements are more accurate than in the standard case. A HPB in the form of a ballistic pendulum would provide the means for a detailed study of the response of the Charpy specimen to various impact velocities. Results already show sensitive rate responses. Thus an extensive set of results could be built up to support the derivation of elasto-plastic fracture parameters.

Only when the accurate measurement of the dynamic response of various notched geometries is possible, can the various material failure criteria be validated. The most realistic aim is to obtain K_{Id} values for varying geometries to a reasonable degree of accuracy, dependent on the data reduction methods used. The next most tractable problem is totally plastic failure under elevated strain rate. In this case various limit analyses and/or numerical programs could be validated by instrumented tests and applicable material failure criteria derived. The characterization of dynamic elasto-plastic fracture, in which cleavage and material rupture interact in a complex manner, remains problematic. Systematic data

could be derived from the Charpy geometry embracing slip line fields and macroscopic parameters. The complex interaction between cleavage and rupture could be studied and empirical relationships derived. But such data would be geometry dependent and hence of doubtful value in the characterization of dynamic failure of complex products.

APPENDIX A
EFD PROGRAM[1]

Fig. A.1 gives the structure and theory of the program. The nodal displacements and velocities are converted into strains and strain rates for each triangular cell, using an averaging method^[2]. These cell strains are then transposed into stresses using the relevant constitutive relation, which depends on strain rate. The stresses in the cells are then redistributed at the nodes to give nodal forces^[2]. The contour selected joins the mid-points of the sides of each triangular cell. This method has been shown to be similar to the Finite Element Method of formulation^[2]. The nodal forces are then added to body forces and external forces. The acceleration at each node, assuming lumped mass at each node, can therefore be derived. An explicit central difference scheme can then be used to derive new velocities and displacements. The cycle is then repeated.

The above methodology should be compared with other formulations. The programs described in [3] and [4] are for purely elastodynamic response and the finite difference scheme is expressed in terms of the equations of motion. HEMP^[5,6] is designed to deal with plastic flow hence the equations of motion are more complex but the methodology is similar to [3, 4].

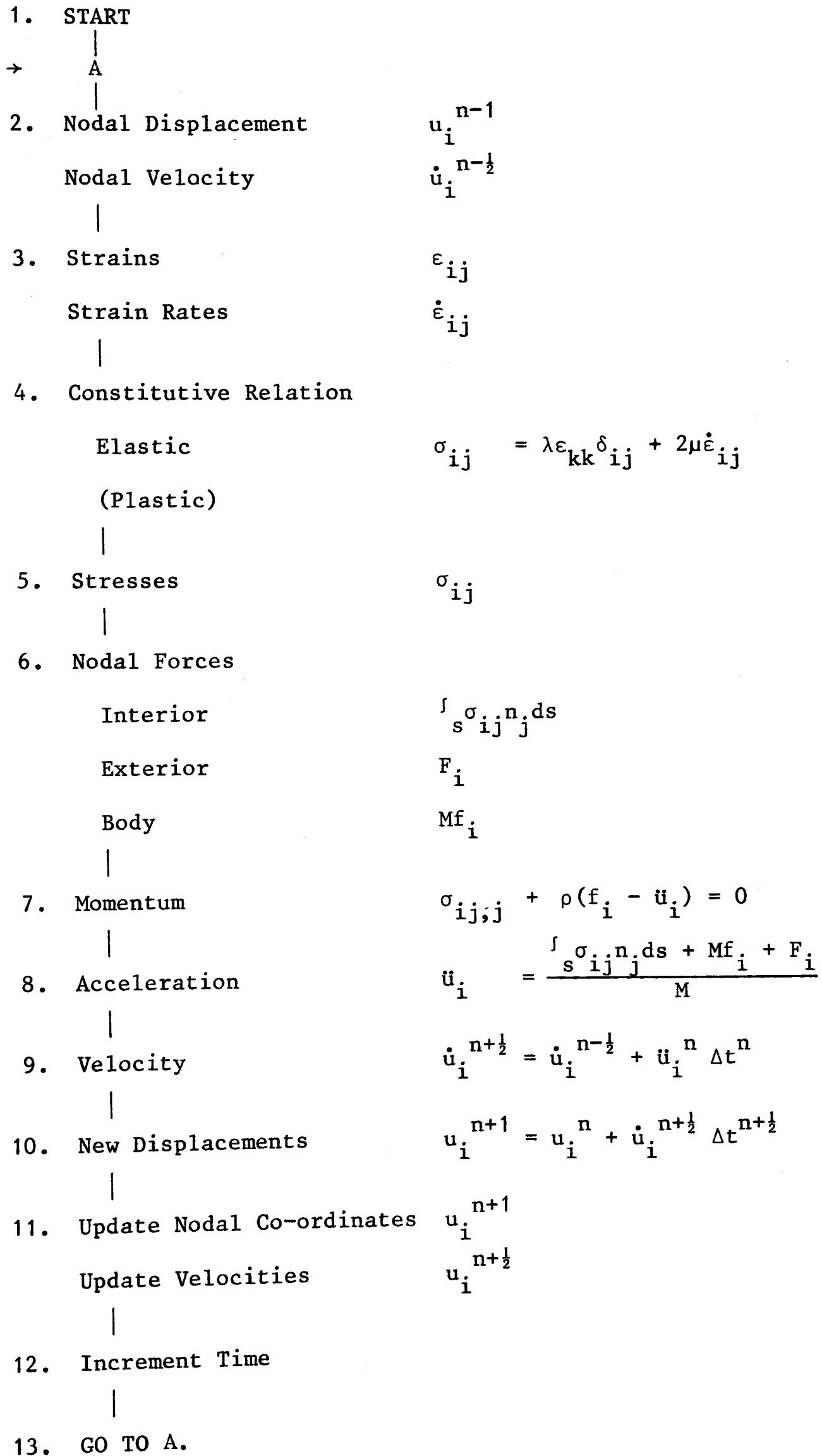
Explicit schemes (step 9 and 10) allow the inclusion of complex constitutive laws and is the usual method for impact loadings^[7]. The time step for integration depends on the size of the smallest element (the Courant Condition) and this can cause problems due to the fine mesh in the vicinity of the notch tip. An updated

Lagrangian procedure is used in which the computational grid is fixed in the material and distorts with it^[8].

The input specification^[9] includes no material damping, a restart facility to enable piecemeal running, and an automatic mesh generation facility. Output can be expressed as an online graph of stress or displacement along the geometry at specific times. The postprocessing capabilities are limited. The program has been implemented on a DEC VAX 11/780 and a typical run takes 1 hour. The restart feature means that execution of the program can be stopped at any time, data stored and restarted at a later date. The program has been extensively tested and many programs run; it was specifically designed for dynamic plastic structural response of 2D or axisymmetric systems. The program, to the authors knowledge, has not been used for fracture problems before.

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Fig. A.1 The Structure of EFD

APPENDIX B

A 1D ANALYSIS OF AN IDEALIZATION OF THE LOADING SYSTEM USED IN THE SINGLE SHOT PHOTOELASTIC CHARPY EXPERIMENTS

The aim of this appendix is to (a) illustrate a typical one-dimensional wave analysis and (b) to give an analytic basis to the strain gauge readings obtained in the photoelastic experiments (see Fig. 5.28). The impacting weight and tup will be assumed to have the same cross-sectional area - in fact the ratio of weight:tup is 1:2 -, the effect of the pointer will be ignored and it will be assumed that there is no specimen. The latter assumption is reasonable given that Araldite is much less stiff compared to steel.

The Lagrangian for the weight - tup system is given in Fig. B.1. The impact gives rise to compressive waves travelling at 5 kms^{-1} . These compressive waves are reflected as tension at the free ends. Fig. B.2A shows the particle velocity at time of impact. Fig. B.2B shows that the particle velocity is $\frac{V_0}{2}$ in the stressed parts, the direction of the particle being in the direction of the propagation of the compressive pulse. In Fig. B.2C the strain gauge registers a load, and the pulse in the weight is reflected as tension. In Fig. B.2D the weight is unstressed with zero particle velocity. The pulse then reflects at the pointer, Fig. B.2E, and the resultant particle velocity is V_0 . Going back to Fig. B.1 the strain gauge signal so far is as shown.

The reflected tensile unloading pulses, with particle velocity in the opposite direction to pulse propagation, then interact - see Figs. B.2F and G. In Fig. B.2H separation of the weight from the tup occurs due to the disparity between particle velocities. Once

the weight separates, in an unloaded state, the stress waves travel back and forth in the tup. In practice, the weight is propelled backwards, i.e. it bounces.

The complete Lagrangian, Fig. B.1, agrees well with stress loads measured, see Fig. 5.28. The motions at the pointer and the strain gauges are given in Figs. B.3 and B.4. In the case of the pointer the focussing of the stress waves at the point is neglected. The net effect is to drive the pointer into the specimen, creating local deformation and causing the specimen to react.

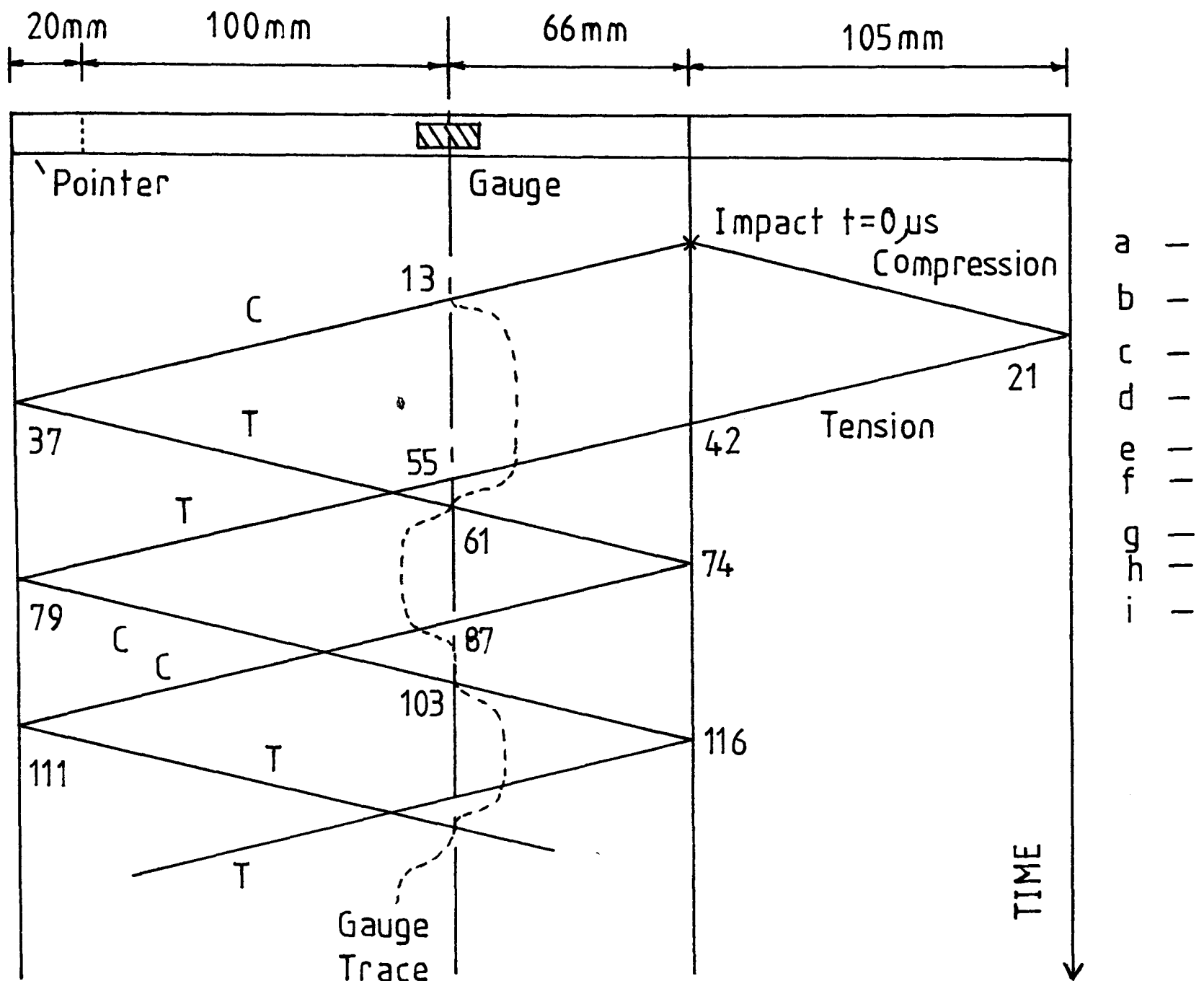


FIG B1

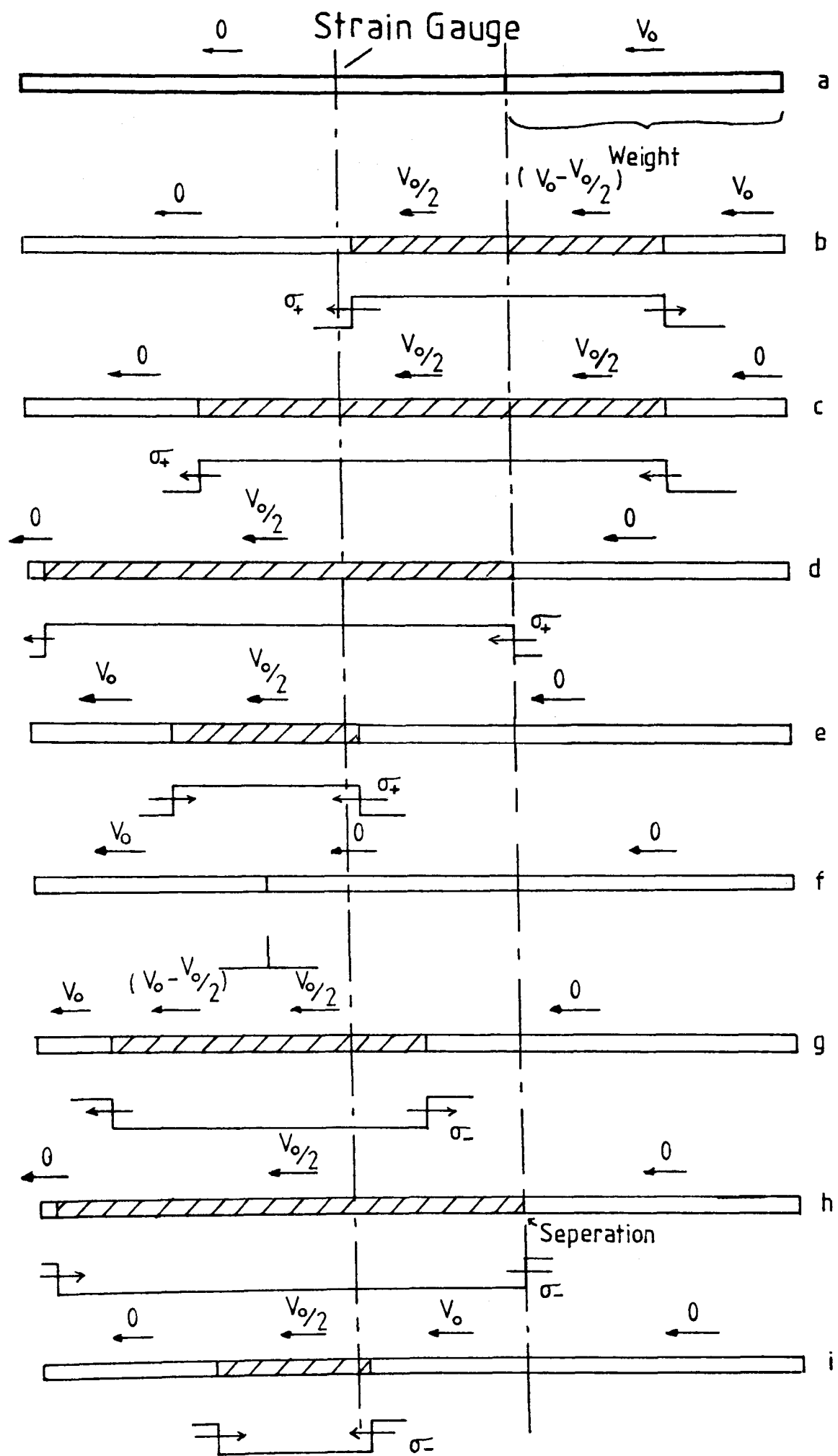


FIG B2

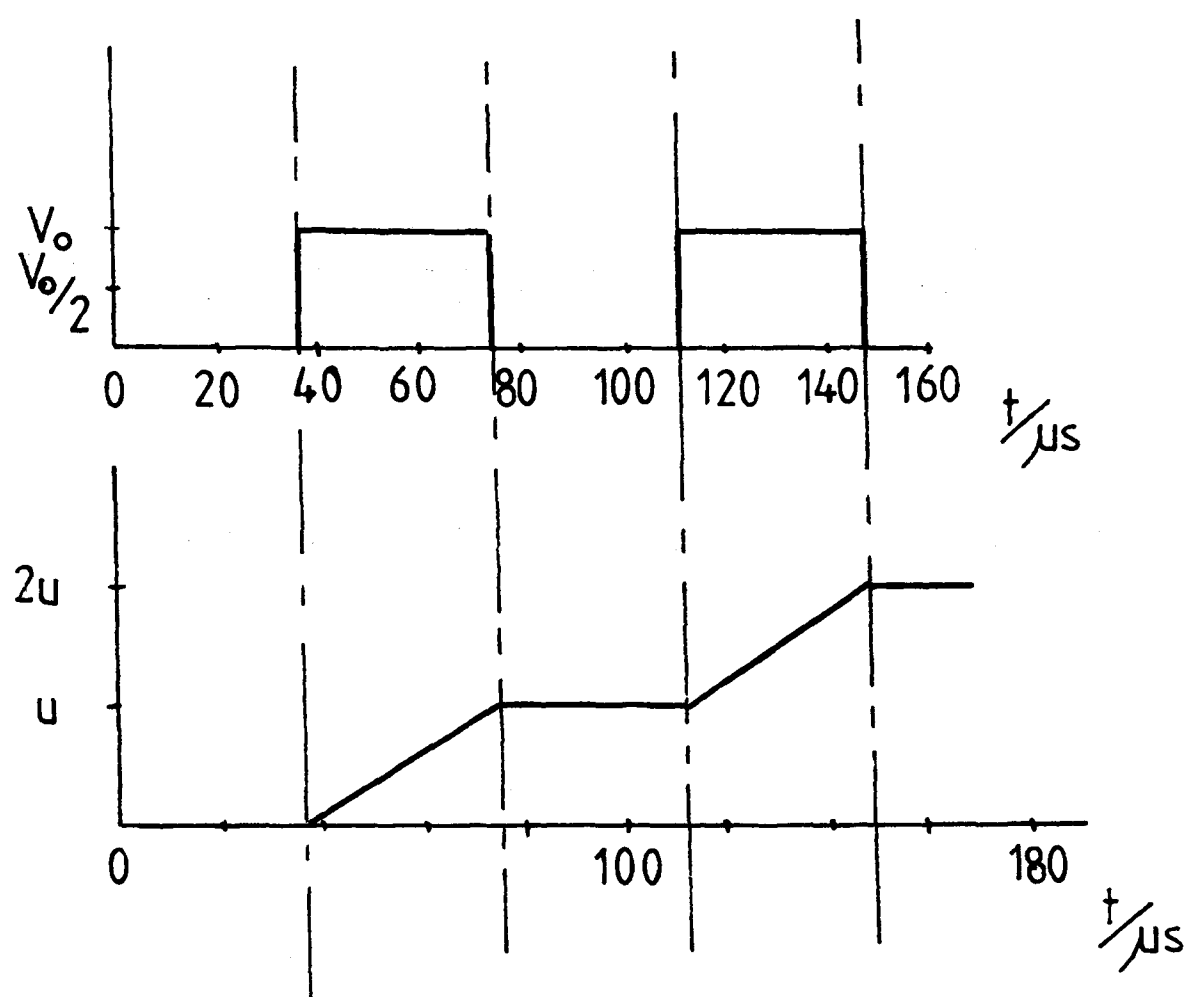
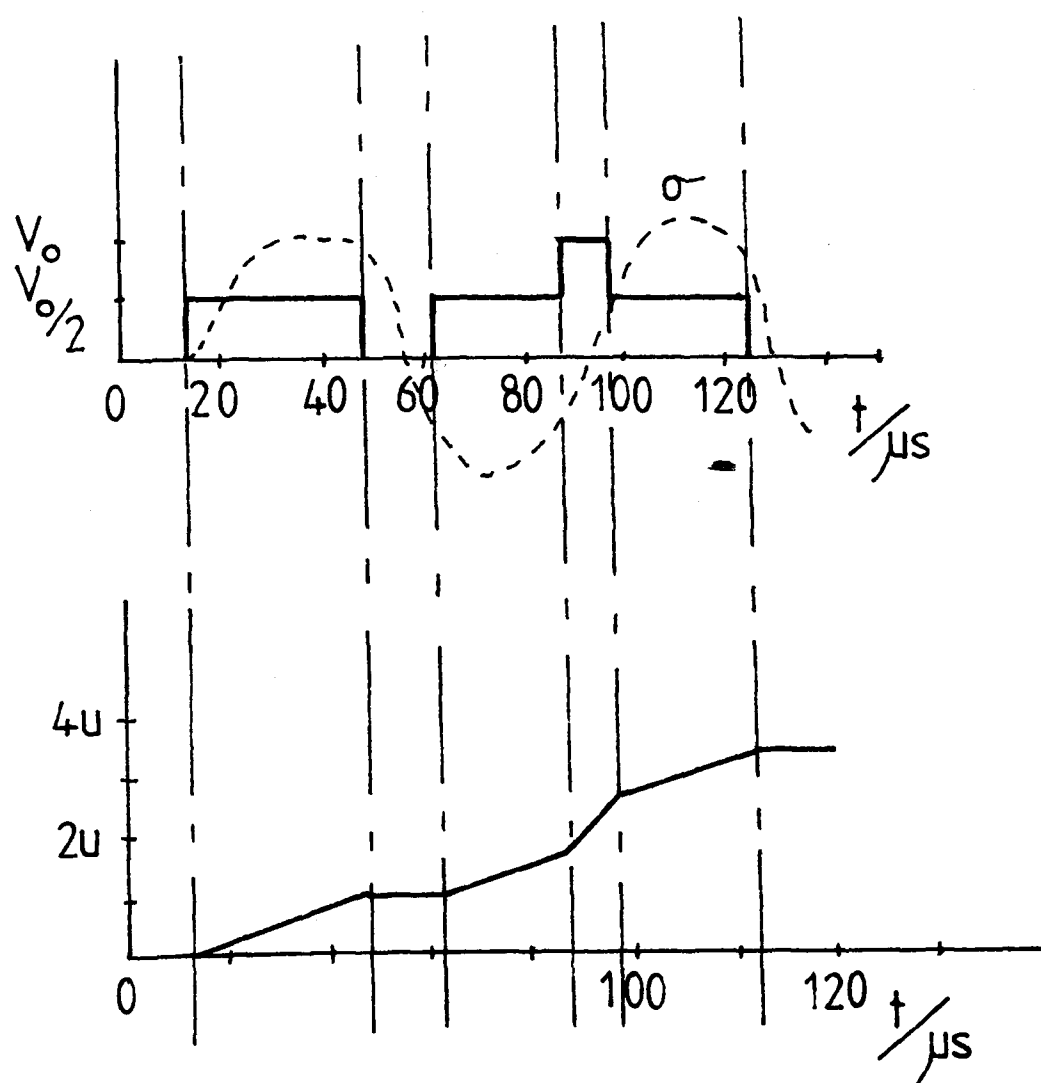
FIG B3: Pointer Displ. u 

FIG B4: Displ. at Strain Gauge

APPENDIX C

DATA REDUCTION SCHEME FOR THE HOPKINSON PRESSURE BAR EXPERIMENTS

Fig. C.1 gives a schematic diagram of the Hopkinson Pressure Bar arrangement. Impact bar A collides with bar B which is held stationary in contact with the specimen. A one-dimensional system of compression waves emanate from the point of contact C. This stress wave behaviour is illustrated by the Lagrangian diagram given in Fig. C.1. The stress pulse passes strain gauges S1 giving a signal ϵ_I (see Fig. C.2). Associated with this stress level is a particle velocity v_I . The stress front then is incident on strain gauges S2, and the growth in stress is the same as for S1 for a time t after the pulse impinged on S2. At this time the strain gauges S2 unload due to the reflections from the specimen and pointer. The reading of S2 is now a combination of the incident pulse, ϵ_I , and reflected pulse, ϵ_R . Thus S1 minus S2 gives the reflected wave. Two wave systems can now be thought to exist in the bar - ϵ_I and ϵ_R . This analysis should be compared with that given in [1].

In the case of strain gauges S2, the trace can be reconstructed from ϵ_I and ϵ_R . Next consider a position closer to the pointer - the time t between ϵ_I and ϵ_R (see Fig. C.2) reduces dependent on twice the time it takes for a wave to travel from this new position to the pointer. In the limiting case, i.e. at the pointer, ϵ_I and ϵ_R start at the same time. This neglects the effect of the pointer (see below). Thus if a strain gauge was 'placed' at the pointer the result of $\epsilon_I - \epsilon_R$ would be the stress at that point - this can therefore be related to the applied force^[1]:

$$P(t) = (\epsilon_I - \epsilon_R) \times E \times \text{Area of Section.}$$

From Appendix B it was shown that the particle velocity doubled due to the reflection of a pulse from a free end. Thinking of ϵ_I as a compressive pulse, with a particle velocity in direction of propagation, and ϵ_R as a tensile or unloading pulse, with the particle velocity opposite to the direction of propagation, it can be seen that the effect of ϵ_R is to add particle velocity to ϵ_I . Thus:

$$v(t) \propto \epsilon_R + \epsilon_I$$

In fact: $\sigma = \rho c v = E \epsilon_T$ therefore

$$v(t) = \frac{E}{\rho c} \epsilon_T = \frac{E}{\rho} \sqrt{\frac{\rho}{E}} (\epsilon_R + \epsilon_I) = c_0 (\epsilon_R + \epsilon_I)$$

Knowing $v(t)$ at any time we can integrate to get the displacement at the section in question. Using the above analysis values of applied force, $P(t)$, and of displacement, $\delta(t)$, can be used to give the work done by the applied force.

The above assumes one-dimensional wave behaviour. This breaks down at the pointer, i.e. at section XY. By correlating ϵ_I and ϵ_R , as described above, we have found the particle velocity and force at section XY. Thus in quoting values of $P(t)$ and $v(t)$ in the main body of the thesis, we have included the pointer with the specimen. Thus values of $P(t)$ are suspect for $t < 10 \mu s$ due to the stress wave behaviour of the pointer. Also some of the CVN energy calculated in Chapter VI goes into accelerating the pointer. For example, in test B3, the kinetic energy of the pointer is 0.3 J - this should be subtracted from CVNE values. Another aspect to mention is that $\delta(t)$ refers to section XY, this is not the same as the deflection of the point of contact of the specimen. The stress wave is focussed by the 'conical' pointer and thus the particle velocity also increases^[2]. It is felt that the above 'secondary' phenomena do not detract from

the results quoted in the thesis, although in testing fatigue pre-cracked specimens, in which the time to fracture and PCVN energy are smaller (see Appendix D), the effect of the pointer becomes greater.

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2. Johnson, W., 1972, Impact Strength of Materials, Arnold, pp. 40 - 46.

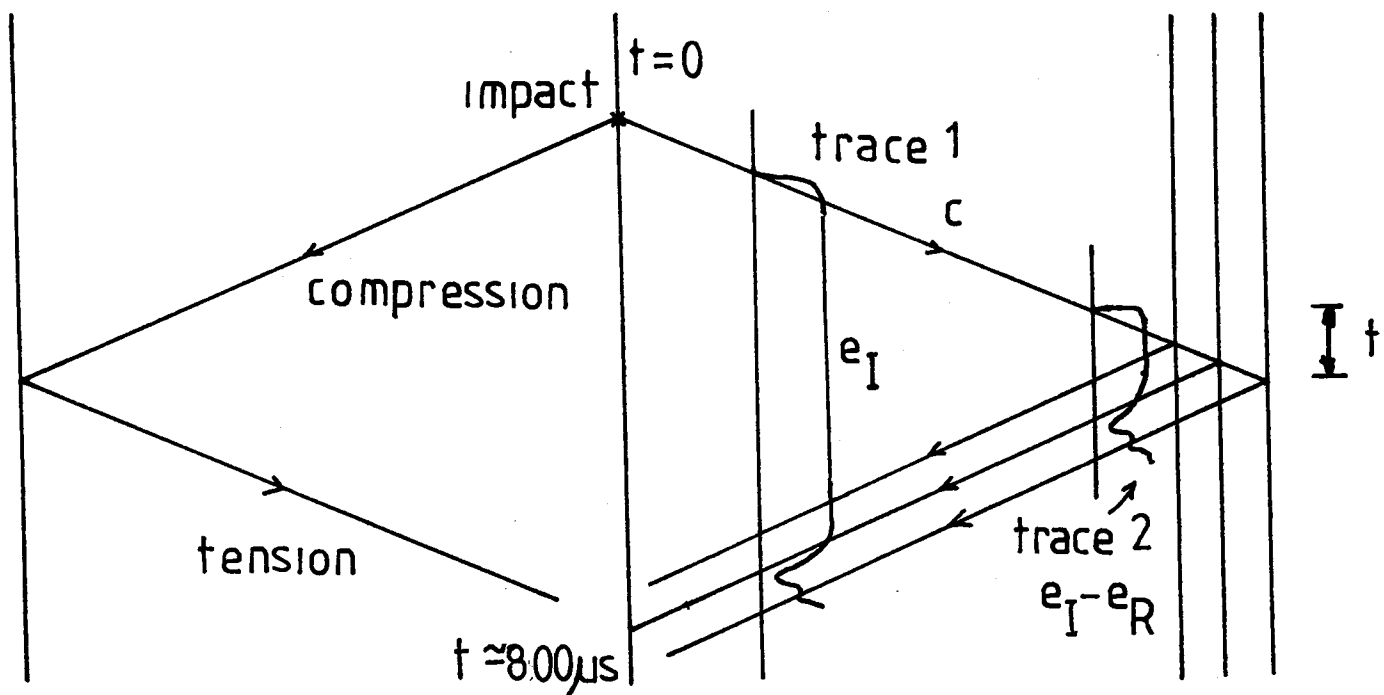
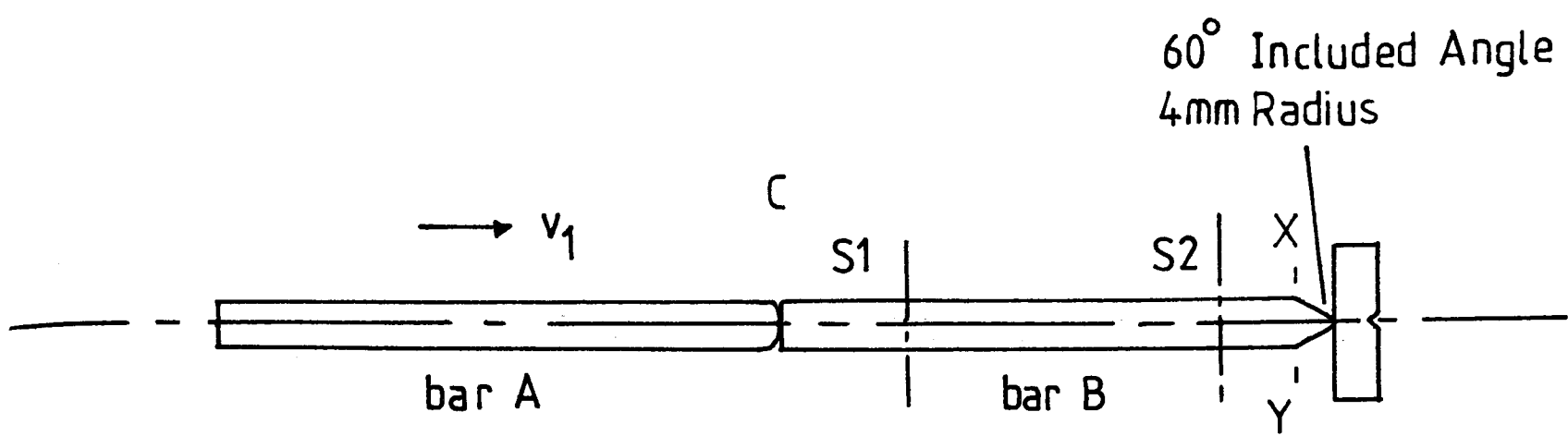
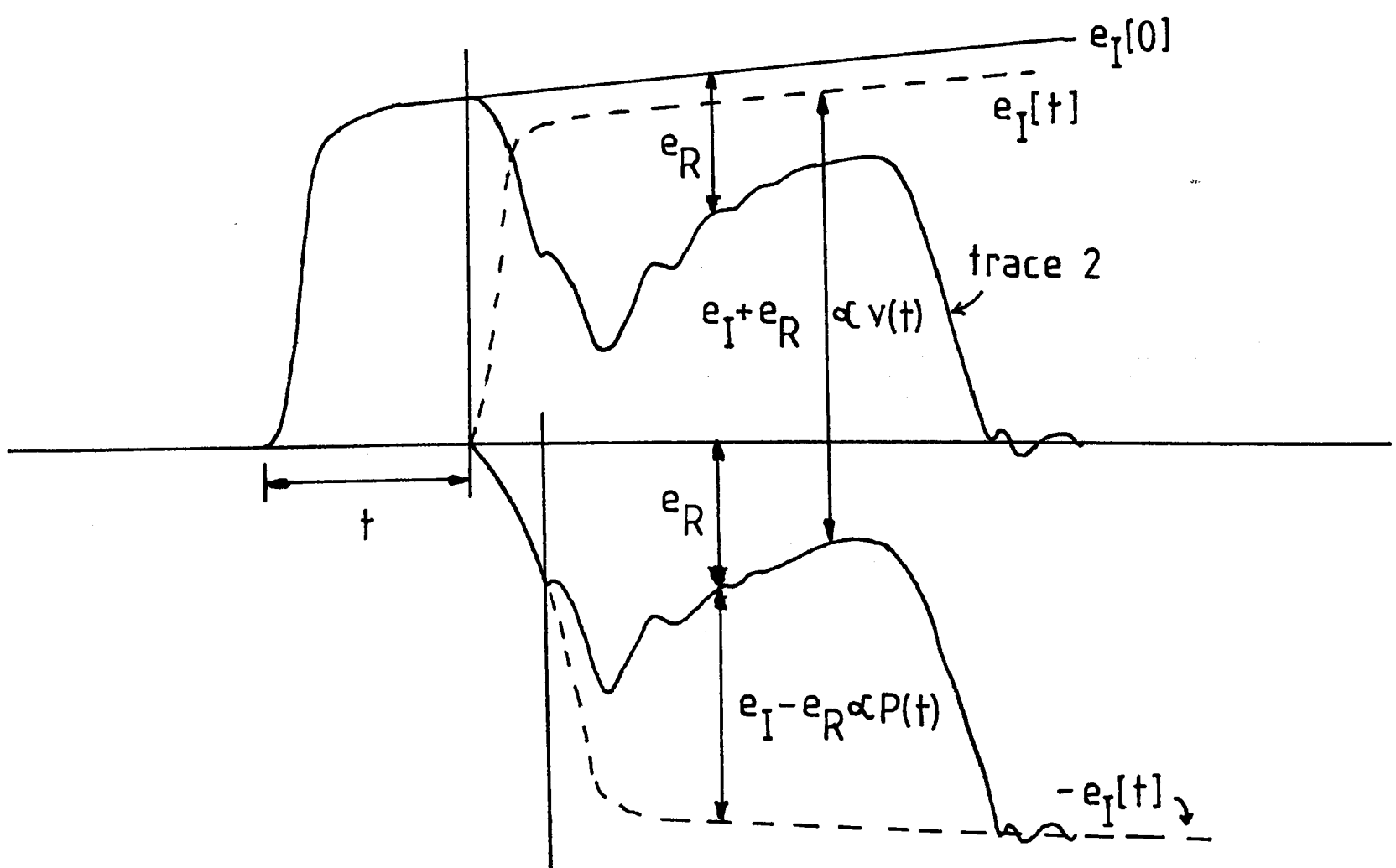


FIG. C.1

FIG. C.2



Appendix D - PCVN results for HPB method of loading

The specimens were machined from a slab of A 508-3 Pressure Vessel at the $5/8 \text{ } t$ level. All specimens were of a LS orientation and so properties are relevant to the assessment of a partial thickness axial defect in a reactor pressure vessel. The composition of the material is given in Table D.1, the heat-treatment in table D.2 and various mechanical properties in Table D.3. The specimens were fatigue precracked to a nominal a/W of 0.5. The fatigue crack was grown avoiding any residual plasticity at the crack tip.

Due to the low loads and fracture times for these specimens an intermediate impact apparatus had to be developed, in between the Nitrogen Gas Gun and the Air Gas Gun. In the event, the 2m long impact bar, shown in figure 6.7, was cut in half giving a 1m long bar and a maximum pulse length of $400 \mu s$. A calibration factor of $24.6 \mu V / MPa$ was assumed for the new set of gauges, S1. Two series of tests were completed : one at $24^{\circ}C$ (Room temperature) and one at $-8^{\circ}C$. In the latter tests, the specimens were cooled in a beaker of methylated spirits placed in a fridge and the tests were carried out to standard ASTM E23.

Figures D.1 and D.2 show the resultant $P (t)$ curves - the data reduction scheme was the same as for the NCB tests. The velocities of impact were varied in the tests to display different types of behaviour - see table D.4. Note that we are working at the lower end of the performance of the Air Gas Gun and the velocities of impact lack reproducibility - for example, tests E1 and E2 were at the same reservoir and barrel pressures.

Tests D1 and D2 were performed at similar impact velocities. The inertial oscillations are similar up until $t = 150 \mu s$, and then the traces diverge. The maximum load for both tests is attained at $t = 168 \mu s$. After this time specimen D1 unloads quickly

whereas the drop in load is less severe for specimen D2. The subsequent plateau corresponds to the load being shed onto the shear lips after 'pop-in'. Both specimens completely fractured during the test (see figure D.3). Reducing the impact velocity, v_3 , from 3ms^{-1} to 2ms^{-1} reduces the inertial oscillations. Note that from HPB theory the applied force is dependent on the impact velocity and hence the impact velocity has been expressed at the fracture load, $(v_3)_m$, and the plateau load, $(v_3)_p$. Crack initiation for test D3 is taken at $t = 168 \mu\text{s}$, when the inertial loading changes into the plateau load. The specimen did not totally fracture in the test. The 'pop in' was highlighted by heat tinting the specimen and fracturing the remaining ligament after the specimen had been cooled in liquid nitrogen. The location of the fracture point is not exact - does the maximum load correspond to the point of initiation?

The value of γ from figure D.1 is $45 \mu\text{s}$, this compares with $\gamma = 46 \mu\text{s}$ from McConnell and Servers empirical criterion (see table 3.1) for an $a/W = 0.5$. Thus $t_f/\gamma = 3.7$ i.e. within Servers '3 γ ' condition, hence a quasistatic compliance can be used to gain a K_{Id} value. The values are given in Table D.4. The UKAEA is currently developing K_{Id} values from Instrumented Charpy Tests, for comparison. From the inspection of fracture surfaces, extensive ductility can be seen to occur and therefore a LEFM concept such as K_{Id} may not be applicable. The fracture toughness of the specimen can be expressed as the work done on the specimen up to fracture, U_f , the effect of the pointer is included and this corresponds to a kinetic energy of 3 - 5% of the total energy to fracture.

The next set of tests were conducted at $t = -8^\circ\text{C}$. Test E1 gives a quasistatic response i.e. inertial oscillations decaying to ramp, the change of gradient of the ramp being due to crack initiation and/or plasticity. Figure D.4 shows that crack initiation

did occur but the location of this point on the $P(\dot{t})$ trace is not possible. Increasing v_3 to 1.5ms^{-1} gives a completely different response. The inertial oscillations are superimposed on a steeper ramp. The transition from the inertial response to the plateau region is well marked - the point of crack initiation can be located with confidence. Figure D.4 shows extensive 'pop-in' and thus should correspond to the maximum load, which approximates to the load at initiation. Further increase of impact velocity leads to a more severe unloading, see test D.3. The fracture of the specimen is completed during the test, see figure D.4. The time to 'pop-in' is similar for both tests E2 and E3. The value of γ is also similar, viz $\tau = 42 \mu\text{s}$ - this is slightly lower than for tests D, but within experimental error. $\frac{t_f}{\gamma} = 3.3$ hence a quasistatic calibration can be used giving the K_{Id} values shown in table D.4. The work done by the applied force up to failure and the contribution of the pointer are also given.

From these few results it can be concluded that for brittle fracture K_{Id} increases with impact velocity; for more ductile fracture, the fracture toughness U_f increases with impact velocity. The rate sensitivity shown in tests E is marked and a systematic study of the test, with the increment of impact velocity being 0.2ms^{-1} and using a ballistic pendulum (see paragraph 7), would yield some more interesting information. The use of the HPB gives an accurate measure of applied force and enables the calculation of deflection and energy values. The location of the crack initiation point is still subjective and approximate. The deflection of the mid point of the specimen is given in table D.4, neglecting the effect of the pointer and plasticity. Further tests will be undertaken at $\dot{t} = 65^\circ\text{C}$, forcing ductile behaviour, and $\dot{t} = -40^\circ\text{C}$, forcing brittle behaviour.

Table D.1 Composition of ASTM-A SA508 Class 3 Low Alloy
Nuclear Pressure Vessel Steel

	<u>Weight % (of the Product)</u>
C	0.2
Si	0.24
Mn	1.36
P	0.006
S	0.007
Ni	0.73
Cr	0.13
Mo	0.48
V	0.01 max
Cu	0.06
Al	0.019
Sn	0.009
As	0.009
Sb	0.0021
Bi	0.001 max

Table D.2 Heat Treatments for Instrumented Charpy Test
Specimens

510 mm thick slab forging
Austenitiz for 11¼ hours @ 870-900°C
Water Quench
Temper for 10²/₃ hours @ 650-655°C
Air Cool
Simulated post weld heat treatment for 21¼ hours @ 605 - 625°C
Air Cool
Specimens machined 5/8 of the thickness from the top in LS orientation.

Table D.3 Mechanical Properties of A508-3 Pressure Vessel Steel

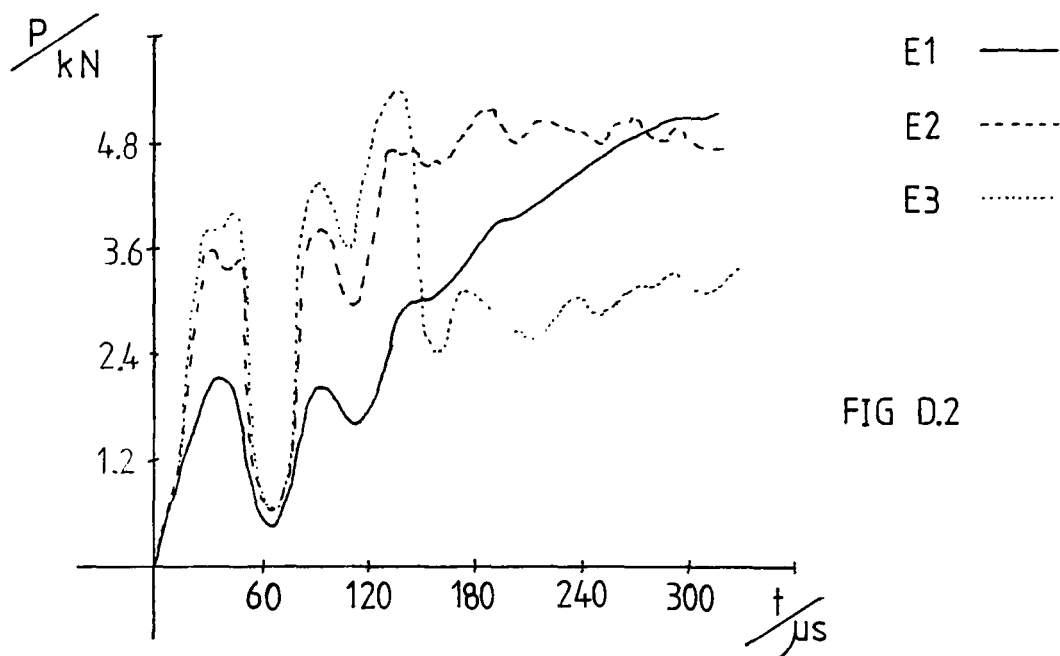
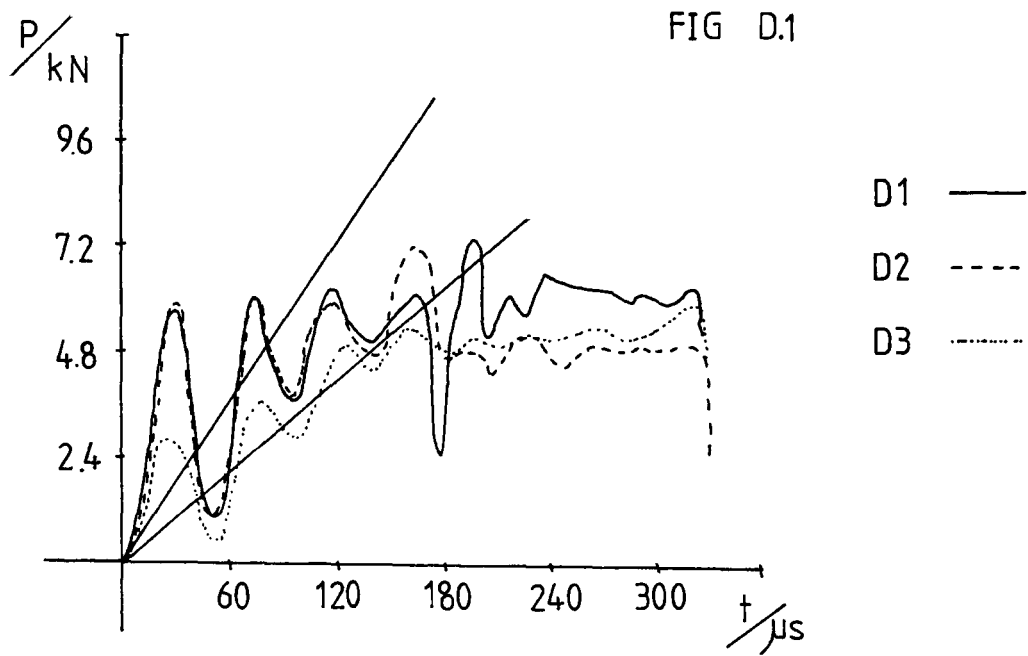
	Room Temp	t = 350°C
$\sigma_{0.2}$ (MPa)	414	339
σ_{UTS} (MPa)	575	528
% elongation	28.9	25.2
Reduction in Area (%)	72.5	65.9

Table D.4 Results from A508-3 Pressure Vessel Steel Tests

Test	a/W *	t (°C)	$(v_3)_m$ (ms ⁻¹)	$(v_3)_p$ (ms ⁻¹)	t_f (μs)	γ (μs)	t_f/γ	P_f (kN)	K_{Id} MPa√m	$\dot{K}_{IS}$ (×10 ⁵) MPa√m s ⁻¹	U_f (J)	$(U_p)_m^\dagger$ (J)	δ_f
D1	0.539	24	2.80	2.90	168	45	3.7	6.13	75	4.5	1.9	0.10	0.44
D2	0.552	24	2.86	3.19	168	45	3.7	7.40	93	5.5	2.0	0.10	0.47
D3	0.531	24	1.63	1.71	168	45	3.7	5.50	65	3.9	0.83	0.03	0.25
E1	0.539	-8	-	(0.9)+	-	42	-	-	-	-	-	-	-
E2	0.535	-8	1.47	1.40	138	42	3.3	4.73	54	3.9	0.51	0.03	0.19
E3	0.522	-8	2.45	2.78	138	42	3.3	5.50	66	4.8	0.93	0.08	0.29

† Velocity v_3 at $t = 138\mu s$.
 * a/W measured at mid thickness

† $(U_p)_m$: Kinetic Energy of Pointer at $(v_3)_m$



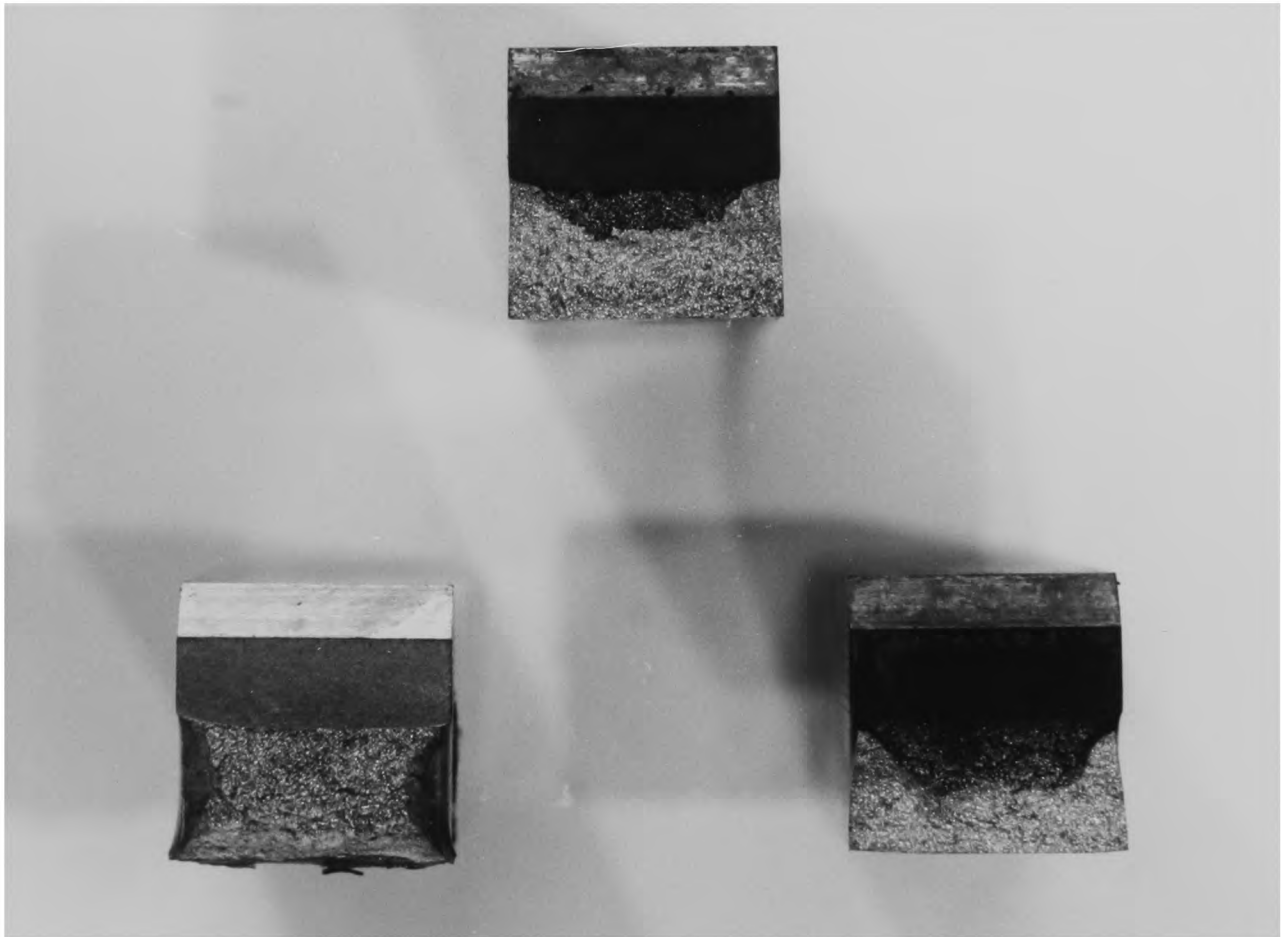


D1

D3

D2

FIG D.3



E1

E3

E2

FIG D.4

Appendix E Specification of the Electronic Instrumentation

1. Datalab DL 922 Transient Recorder

Signal Input:-

Frequency Response	DC to 6MHz
Input Range	100mV to 50V

A/D Converter:-

Resolution	8 bits (1 part in 256) for full scale input
Conversion Rate	20MHz maximum (50 ns/word)

Memory:-

Memory Size	2048 x 8 bit words
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Timebase:-

Sweeptime	100 μ s to 4S
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2. Fylde FE - 351 - UA Universal Amplifier

Input:	$\pm 15V$
Output:	$\pm 10V$
Gain:	x20 to x1000
Frequency Response:	DC to 100 kHz (-3dB) [Modified from 50kHz]
Slewing Rate:	Full output up to 25kHz
Common Mode Rejection:	Greater than 100dB, DC to 1kHz

APPENDIX FPUBLICATIONS ASSOCIATED WITH THE THESIS

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