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THE CITY UNIVERSITY
DEPARTMENT OF MECHANICAL ENGINEERING

A STUDY OF CONVENTIONAL AND HYDROSTATICALLY
ASSISTED DEEP DRAWING OF SHEET METAL

by

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as part of the requirements for the
degree of Doctor of Philosophy

MAY 1981

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ABSTRACT

This thesis examines the effects of the frictional conditions at the blank/punch interface on the success of the deep drawing of axi-symmetrical cups.

The programme was conducted in two parts;

- (a) establishing the frictional characteristics;
- (b) applying an hypothesis to the deep drawing process.

Movement of the material over the punch head was simulated by modified tensile tests enabling material to be pulled around rollers of a given diameter and surface quality. The resulting specimens were then investigated using a scanning electron microscope for the effects of the rollers on the motion of the material. The results were used to predict the behaviour of the materials when subsequently deep drawn.

Axi-symmetrical cups were drawn using plain punches, knurled punches and using a fluid pressure to assist the contact of the blank with the punch. These cups were investigated using a scanning electron microscope for confirmation of the hypothesis from part 1 of the investigation.

It is shown that some improvement in the LDR may be obtained using roughened punches and that some further improvement may be realised by the application of a hydrostatic pressure. Reasoned argument is given to show that this improvement does not depend solely upon the history of the material as given by its n -value which

has been suggested by a number of authors. Rather, it is a complex mechanism depending upon the metallurgical and physical properties of the material and the surface characteristics of both the material itself and the punch. The limitations of the present work are discussed in the light of the results obtained and suggestions are made as to how these may be overcome.

During the study a number of interesting areas of investigation presented themselves and these are discussed under "Recommendations for future work."

Since the completion of the work the author has been invited to participate in the work of the Sheet Metal Technology Group, the British section of the International Deep Drawing Research Group.

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NOTATION

Symbol	Description
A	Constant in the equation $\sigma = A\epsilon^n$
C, D	Constants in the equation $\sigma = C + D\epsilon^q$
CLA	Centre line average
E	Young's modulus
E_o	Plastic buckling modulus
F	Axial force for one Blankholder nut
H_v	Vickers hardness number
LDR	Limiting drawing ratio
N	Number of wrinkles in the flange
P	Total punch force
R_o	External radius of the blank
T	Torque
T_v, T_H	Vertical and horizontal tensions
$\bar{Y}$	Mean yield stress
Y_s	Tensile stress
a	Mean radius of flange
b	Breadth of flange

l_f	Final gauge length
l_o	Original gauge length
n, q	Strain hardening exponent
p	Normal stress due to blankholder
r	Planar anisotropy
r_d	Radius of die profile
r_t	Radius of die throat
$\bar{r}$	Normal anisotropy
$\bar{r}_p$	Effective punch radius
t, t_o	Original thickness of material
w_f	Final gauge width
w_o	Original gauge width
α	Helix angle of thread form
β	Included angle of thread form
σ_b	Stress due to bending over the die profile
σ_r	Radial drawing stress
σ_θ	Compressive circumferential stress
ϵ_r	Radial strain
ϵ_t	Thickness strain

ρ	Density of material
μ	Coefficient of friction
η	Efficiency

CHAPTER 1

INTRODUCTION

The process of deep drawing of hollow vessels from sheet metal has been practised for over a century. From small cylindrical components subsequently to be formed into bellows for control valves through to the large automotive parts requiring mechanical handling equipment, this solid punch process is used extensively.

The principal feature which popularizes the process is that conventional mechanical or hydraulic power presses may be used and the tooling required is basically simple. The material shape itself is simple and the process often results in achieving a final product shape from either a single, or a small number of process operations.

However, although in essence the operation itself can be considered simple the complexity of the pattern of metal flow has resulted in the approximate analytical treatment of axisymmetric cylindrical cups and similar shapes only. Moreover, any work must be presented in a manner suitable for industry to assimilate and use in order to improve the efficiency of the metal forming process.

There are signs that in spite of all the research which has been carried out this aim has not been completely achieved - indeed in 1977 Keeler(1) concluded at a Symposium in Detroit, "I believe very strongly that no adequate solution to the problems of the press shop is available today and it is far too complex for any single

discipline to solve in sufficient time." Keeler went on to suggest that mathematical thought alone will not, of itself, solve the practical problems that industry is faced with. What is required is a synthesis of theory and practice in a creative and scientific manner. Basically the process consists of clamping a blank of material, circular in form for cylindrical cups, between a blankholder whilst a moving punch deforms the material. The clamping of the blank is solely to compensate for the effects of compressive circumferential stresses in the flange which may otherwise cause "wrinkling" of the flange. The effects of this wrinkling would be an unsound product at best, or premature failure of the product due to instability in tension so reducing the degree of possible deformation, as the wrinkles enter the die throat.

Two methods of blank holding are in common use, (a) the so-called constant clearance or plane strain system in which the blank sits in a nest, and (b) a constant pressure or plane stress system in which pressure is applied directly to the blank. Both methods may employ either mechanical or hydraulic means of applying the pressure.

The best known analytical treatment of the subject is by Chung and Swift (2,3) who considered the process in two stages. The first stage, in which a cup is drawn from a circular blank, usually has to be followed by a second or re-drawing stage. This latter stage becomes necessary since the first stage can seldom produce a higher degree of deformation than that defined by a blank

diameter: die throat diameter ratio (the drawing ratio) of approximately 2.2:1 or a cup height:diameter ratio of about 1:1. For a typical commercially pure aluminium it is unusual to attain a drawing ratio much in excess of 1.5:1.

The problem with the work of Chung and Swift, and their contemporaries, is that the subsequent expressions generated by the treatment are extremely complicated. The industry has tended therefore to ignore such rigorous treatments and to rely heavily upon the skill and experience of its artisans.

For the purposes of this introduction, the following explanation is given with reference to Figure 1, which illustrates first stage drawing of a cylindrical cup from a circular sheet metal blank.

The circular blank can be considered to be divided into three zones for the purpose of analysis; A-B, in which the blank is initially in contact with both the die and the blank holder, B-E, in which the blank is not initially in contact with the tooling at all, and E-F, in which the blank is solely in contact with the head of the punch. Downward movement of the punch initiates drawing and the outer rim of the blank is subjected to radial drawing towards the vertical axis of the tooling between the die and the blank holder with frictional resistance present at the blank-tooling interfaces.

Initially a small tensile radial stress concentrated at the blank rim is associated with the applied blankholder load. However, as the radius of the rim is reduced a compressive circumferential stress is induced which causes the blank to thicken. Without the effect of the blankholder this stress may cause the blank to wrinkle.

Bending over the die profile radius results in a further increase in the drawing stress and a thinning of the blank material. Friction between the blank and the die profile increases the drawing stress still further, whilst unbending of the blank at the die throat results in an immediate thinning.

Once in contact with the head of the punch the blank is subjected to stretching and sliding, the amount of straining depending upon the geometry of the punch head and the frictional conditions prevailing.

The foregoing clearly indicates that the process is far from simple and that in order to achieve optimisation of the process a thorough knowledge of the material, tool design and set-up parameters is required. Even when this optimum situation is achieved re-drawing operations are often required in order to obtain the final depth of draw. If the deep drawing ratio could be increased from the currently accepted value of 2.2:1 then considerable savings could be made in tooling set-up, inter-stage operations and hence the unit price of the product.

If this is to be achieved then the failure which can take place in the wall of the cup in the vicinity of the punch profile radius must be suppressed. Thinning of the cup wall in this region may be restricted by clamping the blank onto the punch so that the material moves with the punch rather than being stretch - formed. Such a situation can be brought about by choice of appropriate frictional conditions between the punch head and the blank, and by the application of an hydrostatic pressure on the die side of the blank to hold it securely against the punch.

Very little work has been carried out in this area, most workers concerning themselves with the movement of elements of the blank across the die profile and into the walls of the cup. The effect of friction between blank and die has been considered, but no attempt has been made to quantify its value. If this is true of the blank-die interface less research has been carried out on the effects of friction in the area of the punch-blank interface.

This thesis describes the results obtained by investigating the frictional conditions existing when material is pulled round a radius. Attempts are made to show that the roughness of the punch profile radius is of paramount importance to the value of this coefficient of friction and to its variation during the process. The results obtained are used to predict the effect of punch profile roughness on the success of the deep drawing operation and compared with the results obtained from practical deep drawing experiments.

The principal aims of this thesis may, therefore be summarised as:

- (a) to contribute to knowledge in the field of deep drawing which has not been fully explored to date,
- (b) to present the results of an experimental study concerned with the variation of friction around the punch profile radius,
- (c) to use the results to predict the behaviour of materials which have not been fully investigated, and
- (d) to compare the predictions with experimental results obtained by an experimental programme of conventional and hydrostatic deep drawing experiments.

CHAPTER 2

REVIEW OF PREVIOUS WORK

As well as its importance in its own right as a metal forming process the deep drawing of a circular blank by a flat-headed punch is used as a standard measure of the drawability of a sheet metal.

Although not exclusively adopted it is generally accepted that this measure is defined as the limiting drawing ratio (LDR), defined here as the ratio:

$$\frac{\text{Initial blank diameter}}{\text{mean cup diameter}}$$

Alternatively percentage reduction from the original blank diameter may be used, that is:

$$\frac{\text{Blank diameter} - \text{mean cup diameter}}{\text{Blank diameter}} \text{ per cent}$$

One of the limitations to the LDR approach is that it is purely a GO/NO GO test and does not give any indication as to how near one has come to success. A better technique would be perhaps to use the height of the drawn cup at which splitting occurs expressed as a ratio of the cup diameter. Instability in the top of the cup detracts from this approach to some extent however and the technique is seldom used.

The vast majority of the work in the field of deep drawing has had as its aim, the improvement in the LDR ratio. Considerations have been given to the design of both the punch and the die, particularly the profile

radii of both tools. Attention has been given both to the type of material used and the best metallurgical conditions for an increase in the LDR. Process parameters such as speed of draw, blank holding force and drawing lubricants have been studied, although little attention has been given to frictional ^{conditions} applying around the head of the punch.

The most relevant work to this present investigation is discussed below.

2.1 Effects of Punch and Die Design

An increase in the die profile radius results in an increase in the maximum diameter of blank which may be drawn with a given punch. Although the die profile radius has very little effect upon the punch force, an increase in its value results in a more uniform thickness throughout the component and a reduction in thinning in the base and wall of the cup. Most importantly the "necking" or local thinning in the vicinity of the punch profile radius is reduced. Swift (3) has suggested a radius of up to $10t_0$ may be used whilst PERA (4) report the incidence of flange wrinkling above a radius of $8t_0$. The conditions of frictionless deep drawing are approached with a small punch profile radius and a blankholder force just sufficient to prevent wrinkling. This results in minimum thinning of the blank under the punch head; however necking at the base of the cup wall is increased. Increasing the radius of the punch profile results in a greater degree of stretch-forming being applied to

the blank material under the punch head and consequent thinning of that material. On the other hand a larger blank may be successfully drawn for a small increase in the maximum drawing force. Swift (3) has suggested that where subsequent re-drawing is necessary flat bottomed punches should be used. PERA (4) suggest the use of a maximum punch profile radius of $4t_0 - 5t_0$.

The radial clearance between the punch and the die may be less than or greater than the thickness of the blank material. If the former is adopted, ironing takes place which eliminates the variation in wall thickness and improves the surface finish. There is, however, an increase in the maximum drawing force and the maximum value of depth of draw possible is reduced. Sachs (5) has suggested a radial clearance value of $0.4t_0$ whilst Chung and Swift (3) suggest $0.3t_0$ for general purposes reducing to a value as low as $0.1t_0$ if ironing can be tolerated. Barlow (6) advocates radial clearances of $0.2t_0 - 0.1t_0$ stating that the additional drawing force resulting is not excessive and occurs near the end of the stroke where the drawing force is well below its maximum value. There should therefore be no reduction in the maximum depth of draw possible. He further suggests a minimum value of $0.05t_0$ for radial clearance before the maximum reduction is affected. If the above parameters are not kept to within reasonable limits then one or other of a number of failures may occur,

2.2 Types of Failure

- (a) Due to the bending and unbending of the blank material at the die profile radius and its consequent thinning, failure takes place at the neck in the cup wall near to the junction with the bottom punch profile radius. The severity of this necking is dependent upon the die profile radius described above and also upon the rate of work-hardening of the material. This type of failure is demonstrated in conventional deep drawing by materials which have been severely cold worked. This type of failure does not occur further up in the wall of the cup due to masking of the thinning by the circumferential compression which produces a thickening of the flange material as it is drawn inwards across the die face.
- (b) The most common type of failure, and one which has received the most attention, occurs over the punch profile radius in the vicinity of the top of the neck. Assuming that all conditions are favourable then failure takes place at this point when too great a reduction is attempted. Sheet metals with a high rate of work-hardening tend to exhibit this type of failure.
- (c) Two other types of failure may occur, being mainly material dependent rather than tooling dependent. Materials exhibiting insufficient ductility in

simple compression tend to fail in the flange in which substantially pure compression takes place during the drawing process. This type of failure occurs in the form of a 45° shear crack in the flange. Tensile failure may also occur high up in the wall of the cup after the maximum drawing force has been exceeded. Under the action of the applied radial tension and the induced circumferential compression, the material high up in the cup wall undergoes a natural strain of up to 0.47. The induced circumferential compression delays the point of tensile instability. However, brittle materials are unable to withstand this extension and failure takes place

The first published work into flange wrinkling was by Baldwin and Howard (7) but the first experimental work was not carried out until 1951 in a paper published by Sanders and Loxley (8). However, the most rigorous work was carried out by Senior (9) (10) who used an energy method of solution assuming a sinusoidal deflected shape for the rim.

Senior's simplified analysis treated the problem as essentially an elastic one but with the elastic modulus replaced by a plastic buckling modulus. Using such an analysis Senior obtained a minimum and a maximum value for the number of wrinkles

$$N_{\min} = 1.65 \, a/b \quad \text{and} \quad (1)$$

$$N_{\max} = 2.33 \, a/b \quad (2)$$

Alexander (11) queries the latter, which is also quoted by Johnson (12), stating that the maximum number of wrinkles should be;

$$N_{\max} = 2.08 a/b \quad (3)$$

The onset of wrinkling occurs when the flange becomes unstable and Senior showed that the limit for stability is given by;

$$\sigma_{\theta} = 0.46(t/b)^2 \quad (4)$$

The plastic buckling modulus used by Senior was that developed in 1909 by von Karman (13) namely;

$$E_o = 4EP/(\sqrt{3} + \sqrt{P})^2 \quad (5)$$

The effect of the blankholder is to increase the number of wrinkles with a consequent reduction in amplitude. The load should be sufficiently high not only to affect the above condition but also to prevent the blankholder lifting due to coning which leads to wrinkling in the latter stages of drawing and subsequent failure.

More recent work by Naziri and Pearce (13) showed that the blankholder pressure necessary to suppress wrinkling increases with a decrease in normal anisotropy and increases with an increase in planar anisotropy. These authors also showed that the number of wrinkles produced decreases with an increase in the drawing speed.

2.3 Factors Affecting Limiting Drawing Ratio

The results of all this work has led to the establishment of certain predictable values concerning the limiting drawing ratio. Swift (3) has shown that for most common materials the value of LDR in conventional deep drawing can vary from 1.5:1 to 2.2:1. Barlow (6) following his extensive work on commercially pure aluminium refers to an LDR of 2 as being very good whilst an LDR of 1.67 can be considered as poor. It should be noted that an accuracy of 10% is considered necessary to cover variations which might exist or occur, for example, in material properties and lubrication. The largest reduction likely to be obtained in practice with conventional tooling is 50% or an LDR of 2:1.

2.3.1 Material ductility

Such values of limiting drawing ratio require materials to have sufficient compressive ductility to withstand these large reductions from the initial blank diameter. Generally speaking, if a material satisfies this criterion it will also have adequate tensile ductility to avoid upper-wall failure and be able to withstand bending over the die and punch profiles. Greater ductility in compression than in tension is called for in a deep drawing material and hence tensile ductility is a poor measure of deep drawability. Hence the ultimate tensile strength should be high in relation to the compressive properties since this determines the ability

of the wall of the cup to withstand the drawing force brought about mainly by the compressive properties of the material.

2.3.2 Material Anisotropy

Although work has been performed for some time previously to relate the material properties to the success or failure of the deep drawing operation, it was not until 1960 that Whiteley (15) showed a correlation between LDR and the normal plastic anisotropy of the material.

In an appendix to Whiteley's paper it is clearly shown that it is anisotropy rather than the levels of strength or ductility which governs the drawability of a material. Whiteley proposed a relationship of the form;

$$\ln (D/d) = 1/[1+n\{(1+r)/2\}^{\frac{1}{2}}] \quad (6)$$

However, Wright (16) has shown that this correlation may not be accurate when other material characteristics contribute to the failure. The measure of anisotropic plasticity is generally accepted as being the ratio of the natural width strain of a specimen divided by its natural thickness strain. This ratio was employed by Lankford (17, 18). Nevertheless, due to the difficulty in measuring thickness strain, Burns (19) proposed a ratio of the form;

$$r = \ln (w_o/w_f) / \ln (w_f l_f / w_o l_o) \quad (7)$$

This ratio assumes that the material is incompressible throughout and has been widely adopted by subsequent workers in the field.

Atkinson (20) has proposed that the drawing force is likely to offer the best prospect of a simple testing procedure that can be used to evaluate normal anisotropic plasticity from one test result. Other methods involve the investigator in carrying out a very large number of tests in order to obtain a meaningful average value. Distinction must be made between normal anisotropy and planar anisotropy. The former relates to the difference between the material flow strengths in the plane of the sheet and normal to the plane of the sheet. Such anisotropy is often a desirable property of a deep drawing material since it is an indication of the resistance to thinning of a material and hence its ability to resist premature failure. Planar anisotropy on the other hand is a measure of the material flow strength in different directions within the plane of the sheet. Its existence gives rise to the formation of so called "ears" in the top of deep drawn cups resulting in a further operation to trim the top of the cup. These ears are formed in the direction in which the strain ratio is a maximum, that is a positive value of planar anisotropy indicates the formation of ears at 0° and 90° to the rolling direction whilst a negative value is indicative of earing in a direction at 45° to the rolling direction. The ideal material for deep drawing is therefore one in which the normal anisotropy is greater than one and for which the planar anisotropy is zero.

Woodthorpe and Pearce (21) studied the work of Keeler (22) and drew conclusions as to the effect of normal anisotropy on the shape and level of the Forming Limit Diagram. They showed that as the normal anisotropy reduced so does the largest principal surface strain and the surface strain perpendicular to it.

2.3.3 Formation of Ears

Wilson (23) has provided a qualitative prediction of the formation of ears in deep drawing. Jimma (24) has brought some mathematical rigour to the problem and the work has been further extended by Sowerby and Johnson (25) by the use of slip-line theory. The work of Sowerby and Johnson uses the now universally adopted work of Hill (26, 27) for describing the state of anisotropy.

2.3.4 Work-hardening Index

Keeler (28) and Woodthorpe (21) have both shown that the work-hardening index of the material has a significant effect upon the subsequent success or failure of a deep drawing operation. The characteristic may be represented by an equation of the form;

$$\sigma = A\epsilon^n \quad (8)$$

Swift (3) has suggested a modification to the above equation for use with materials which exhibit a marked tendency to work-harden. He proposed an equation of the form;

$$\sigma = C(D + \epsilon)^q \quad (9)$$

The primary influence of the work-hardening index is to improve the uniformity of the strain distribution in the presence of stress gradients. This leads to overall higher strains through a postponement of localised straining. Thus the greater the value of this index, the more uniform the strain distribution becomes and the deeper the draw possible. The influence of the work-hardening index is greater than the influence of the material anisotropy.

Kals (29) has shown that an increase in the value of the work-hardening exponent results in an increase in the limiting drawing ratio. This is in marked contrast to Arbel (30) who concluded that the new alloys with a high work-hardening content would not show any improvement in the limiting drawing ratio. Kals concluded that too much importance placed in one material led Arbel to the reverse conclusion.

2.3.5 Punch Speed

The effects of punch speed have been investigated by Coupland and Wilson (31). The problem is complicated by the effect that speed has on both the properties of the blank material and upon the lubrication. Generally speaking with a flat-headed punch the limiting drawing ratio increases with speed, almost wholly due to the improvement in lubrication. With hemispherical punches, however, there is no such increase in the limiting drawing ratio and invariably there may be a reduction as punch speed increases.

Speed effects are less pronounced when using solid lubricants such as graphite and more pronounced when using liquid lubricants such as mineral oils. The addition of an extreme pressure additive to the lubricant appears to have no effect upon the process at normal drawing speeds and in the absence of ironing.

2.3.6 Frictional Conditions

It has been shown by Wallace (32) that the use of a roughened punch can increase the limiting drawing ratio from 2.33:1 to 2.53:1 when using aluminium stabilised, extra deep drawing quality mild steel. This increase is thought to be achieved due to the increase in friction between the blank material and the punch, particularly in the vicinity of the punch profile radius. Over the radius the cross section of an annular element of material decreases towards the pole with a consequent increase in the radial stress. Since frictional forces act away from the pole, an increase in friction resulting in a reduction in thinning transfers the failure zone to the wall of the cup. The cross section of the wall of the cup being greater than the cross section of the cup over the punch profile radius results in an increase in the limiting drawing ratio. El-Sabaie and Mellor (33) in their later work on commercially pure aluminium and 70/30 brass confirmed these results but with reduced advantages in the limiting drawing ratio.

Industrially, there are disadvantages in attempting to keep the punch contact area of the blank clean whilst applying lubricant to the die side of the blank. In his work, Wallace also concluded that an increase in drawing ratio obtained by differential lubrication decreased with punch roughness, the largest ratio being obtained with a knurled punch and a blank lubricated on both sides. Whilst obviously having industrial advantages if true, it is difficult to reconcile this result with the need to increase the friction over the punch head. Recognising the importance of this frictional condition, attempts have been made by Lange and Dalheimer (34) and Ghosh (35) to overcome the predictions obtained from the conventional slider type tests. Duncan et al (36) proposed a tensile strip test for evaluating friction under conditions which simulate stretching of the sheet over the punch corner radius in forming shallow stampings in a draw die. Limited work has been carried out in this area, Duncan's work itself being limited to a selection of aluminium alloys used in automotive applications.

The technique adopted by Duncan was also suspect at the higher values of strain. By concentrating upon load rather than stress in the material he ignored the significant effects of the change in the cross sectional area of the material.

Fukui et al (37) investigated the effects of surface roughness of both the tooling and the sheet metal on

deep drawability. Results clearly indicated that an increase in the surface roughness of the die, blankholder and the blank had the effect of decreasing the frictional resistance over the flange portion of the blank. They postulated that this was due to the trapped lubricant in the pockets of the material surface being effectively supplied to the projections of the uneven surfaces. However, in the case of less viscous lubricants, the metallic contact or boundary lubrication increases, so increasing the frictional resistance since the lubricant is not so effectively applied.

In the area of the punch head the rupture strength of the material is affected by both the shape of the punch head as discussed in Section 2.1, and the frictional resistance over the contact surface of the punch and blank. In his work, Fukui found that with a rougher punch surface, higher drawabilities were possible. The effects described were dependent upon viscosity of the lubricant and the drawing speed.

With viscous lubricants a dull finish on the punch gave improvements at both low and high drawing speeds. However, with less viscous lubricants a high drawing speed has insignificant effect and when the punch roughness exceeds $10\mu\text{m}$ CLA the deep drawability was lowered. With low forming speeds and less viscous lubricants an insignificant effect was noted at roughness values lower than about $10\mu\text{m}$ CLA.

2.3.7 Pressure Assisted Drawing

In 1956 an exciting technique was published by Maslennikov (38) to improve the limiting drawing ratio from between 2:1 and 2.5:1 for aluminium alloys to between 6.5:1 and 7:1 using multi-draws. These results were obtained by backing the blank with a rubber ring restrained within a rigid container. As the punch descends, the rubber ring moves radially inwards dragging the blank with it towards the die cavity. After each stroke the load is removed allowing the ring to recover before the cycle is repeated. The pressure of the rubber ring replaces the effect of the blankholder force and, if the dimensions are correct, little or no wrinkling occurs. Thinning of the blank, particularly in the vicinity of the punch profile radius is zero. The process is not without difficulty, particularly with respect to its control, and many workers have been frustrated in their attempts to meet with success. However, Derweesh and Mellor (39) were able to show that polyurethane with its better wear and recovery properties was a far better proposition as a ring material. The hardness of the polyurethane together with correct lubrication are other critical factors in achieving success. According to these authors the main disadvantage with the process is that the drawn material thickens considerably requiring a subsequent ironing process.

In an attempt to improve the LDR by a reduction in friction, Kasuga (40,41) investigated the possibility of applying the lubrication under pressure.

This was achieved by filling the die aperture behind the blank with a liquid lubricant. As the punch descends to commence the draw, the fluid having small compressibility, is caused to escape over the die radius and into the upper side clearance between the die and the blank. In this manner it was hoped to establish almost perfect lubrication. Kasuga claims an increase in the limiting drawing ratio for annealed commercially pure aluminium of from 2.08:1 to 2.58:1. Kasuga "was quick to realise" that the main reason for the increase in limiting drawing ratio was due to the hydroforming - like straining rather than to the reduction of the drawing force brought about by the pressure lubrication. Kasuga was also of the opinion that an initial pressure is required for optimum results prior to the commencement of the draw. This initial pressure has the effect of seating the blank material firmly on the head of the punch so restricting the tendency for stretch forming to take place. A process developed by Schuler was described by Buerk (42). Basically similar to that proposed by Kasuga it uses water as the pressure transmitting medium together with a seal in the face of the die to produce a closed compartment. The subsequent advance of the punch increases the pressure in the compartment, the maximum value being limited by a pressure relief valve. The pressure clamps the material against the punch but also forms up that portion of the blank unsupported by either the punch or die into a bead. Drawing takes

place over this bead and not over the die radius. The material supporting the drawing force has therefore only to transmit sufficient force to allow material to flow from this bead. El Sabaie and Mellor (43), working along similar lines to Kasuga et al, showed that a 57% increase in the limiting drawing ratio for 70/30 brass could be achieved. They proposed a theoretical analysis describing the instability conditions and showed that comparison with experimental results was good. Using an up-stroking machine it was necessary to empty the pressure chamber after each draw using a vacuum pump. An air-hydro pump was used to subsequently fill the chamber with oil to a fixed pressure. Their results indicate that, under certain conditions, it is possible to suppress failure under plane strain tension in the wall of the cup so obtaining a greater limiting drawing ratio. These conditions result in material emerging from the die failing under conditions of uniaxial tension. Using this method they state that the largest increases in the limiting drawing ratio will be achieved for a material having a high n -value and a low r -value. For a given value of r the process proves more beneficial as the value of n increases. Whilst El Sabaie and Mellor restricted their work to 70/30 brass, Ng et al (44) continued the work using an aluminium killed steel which had an n value of approximately 0.22. They found an increase in the limiting drawing ratio over that which could be obtained by conventional deep drawing, but the

increase was small, less than 10%. To obtain such an increase the conditions of the application of the pressure were changed from that used by El-Sabaie and Mellor. Instead of applying an initial pressure which was allowed to rise as the process proceeded they applied the full pressure at the commencement of the operation. This was explained by the different rates of work-hardening between the 70/30 brass and the killed steel. For the brass, the maximum drawing force occurs later in the draw thus necessitating a slower rate of pressure build up than for the killed steel.

Ng et al also introduced into their work a third mode of failure referred to as balanced biaxial tension which is assumed to be failure at a radius equal to the punch stem radius under biaxial tension. Their theory predicts that such failure will occur in materials which exhibit an r value of 1 or less, such that for materials having an r value of 1, failure in balanced biaxial tension will occur for materials with n values between 0.05 and 0.24. For materials with an r value of 0.5 failure will occur with an n value less than 0.4. For materials having r values between 0.5 and 1, the limiting drawing ratio is shown to be quite insensitive to changes in the n value of the material.

CHAPTER 3

DESIGN OF EXPERIMENTAL EQUIPMENT

3.1 Universal Testing Machine

The deep drawing work was carried out on an Avery universal testing machine, model No. 7110 DC. This machine consists of a base unit housing the hydraulic cylinder and ram, a work table and a moveable crosshead. The latter feature was modified to suit the height of the tooling.

The machine is capable of a maximum load of 250 kN over five load ranges, the load being indicated by a pointer capable of being read to an accuracy of 0.25kN full scale deflection. Before use the machine was operated to achieve a working temperature for the hydraulic oil when a fairly consistent range of table speeds is possible using the handwheels provided.

3.2 Design of the Experimental Tooling

3.2.1 General Assembly

In designing the experimental tooling, attention was paid to the need to produce a piece of equipment which could be used for both conventional and hydrostatically assisted drawing, and also for the range of materials to be tested. There was in addition a requirement to be able to maintain the appropriate clearances between the die and cup to ensure that minimum ironing of the cup wall occurred.

To these ends a standard pressure chamber was designed which could accomodate the maximum length of cup that it was estimated would be produced. On top of this chamber was assembled a die plate which was bored out to

accept the die. This feature was adopted in order that the die could be extracted either for refurbishing or replacement. Conventional 'O' rings were used between the die and die-ring interface and between the die-ring and pressure chamber interface.

The die itself was bored out to accept a Dowty bonded seal, see 3.2.2, and the back of the die drilled to facilitate removal of this seal. The holes were tapped and sealed using M8 grub screws to prevent the ingress of oil.

The whole assembly was mounted in a welded steel oil tray which collected any oil displaced from the pressure chamber and returned it to the oil reservoir via a plastic pipe.

All pipe runs from the pressure relief valve to the pressure chamber were piped in stainless steel.

3.2.2 Punch and Die Design

Both the punches and the die were manufactured from En 24T. In order to remain within the capabilities of the universal testing machine with the range of materials to be used, a punch diameter of 38 mm was chosen. A punch profile radius of 4 mm was used in the work which was considered to be the optimum value for the slight variations in material thicknesses to be investigated. Two punches were manufactured, one being ground all over, the other with a medium coarse knurl applied over the punch head and around the profile radius. Both

punches were given a taper of 1 in 1000 on the shank to facilitate removal of the successfully drawn cups.

To obtain a value of punch force an integral dynamometer was designed situated between the punch shank and the crosshead of the testing machine. A full bridge circuit was used employing 120 ohm strain gauges giving full compensation for homogeneous temperature change.

To allow the use of a single die for the range of materials and sheet thicknesses a die aperture was chosen of 39.4 mm. A profile radius of 5 mm was used, this being approximately ten times the average material thickness used throughout the tests. After grinding, the face of the die and the profile radius were hard-chromed to produce a good quality, hard wearing surface.

To ensure sealing of the die aperture during the hydrostatic drawing tests, a Dowty bonded seal, type 400-019-4490-02 was fitted into the face of the die. The seal was located in an accurately machined groove positioned close to the die profile radius. The seal itself was machined to achieve a good seal without the application of too high a force to the flexible lips of the seal. A close-up view of the die surface with the seal in place is shown in the photograph of Figure 2.

3.2.3 Blankholder

A pressure type blankholder was used during the study, the force being applied by six equally spaced M8 bolts. As in the case of the punch, an integral dynamometer was designed using 120 ohm gauges. A full bridge circuit was

employed giving full compensation for homogeneous temperature change.

The face of the blankholder was accurately ground and hard-chromed to give a good quality hard wearing surface.

3.2.4 Material Testing Equipment

Testing of the various materials to obtain their properties and true tensile stress/natural tensile strain relationship was carried out on a Hounsfield Tensometer. The equipment was arranged to give a plot of the load applied to the test piece against the extension of the test piece.

The same piece of apparatus was used to carry out the friction tests; in this case the machine was set up on a concrete surface table at an inclined angle of 20° . The material was bent around a specially designed holding device which was capable of accepting non-rotating rollers. The two other ends of the test piece were gripped in a double jaw designed to fit onto the crosshead of the tensometer. Two sets of rollers were produced both 10 mm diameter to simulate the punch profile radius; one pair were hardened and ground to simulate the surface condition of the plain punch whilst the other pair were similarly provided with a medium knurl to simulate the rough punch.

Strain in the short length of the specimen was recorded using an extensometer, whilst the strain in the long leg, was recorded by an optical travelling toolmaker's microscope. A plot was taken at the same time of the

load/extension characteristic of the test piece. A general view of the apparatus is shown in the photograph of Figure 8.

3.3 Pressure Chamber

The chamber was manufactured from mild steel and designed to accomodate a factor of safety of 2. A base flange enabled fixing to the machine table and the die was bolted down onto the chamber using three equally spaced M6 bolts. A hydraulic seal was obtained between all mating surfaces using 'O' ring seals.

The chamber was filled with oil and pressure applied via an "Enerpac" electric pump controlled by a Program Control Centre type BIC-94. This control console allowed pre-pressurising of the chamber to a pre-determined value prior to the drawing taking place. An added advantage of the system was that the time of the application of this pressure could be automatically controlled.

The maximum pressure attained in the chamber was limited by a Towler Hydraulics pressure relief valve type 25WB3316M. This valve allows pressures to be pre-selected up to a maximum of 82.74 MPa.

A general view of the apparatus is shown in the photograph of Figure 3.

3.4 Recording Equipment

Records of punch force, punch travel, blankholder force and chamber pressure were all recorded on an

SE, U.V. recorder, type 2112. The output signals from the punch and blankholder load cells were first fed into an SE Transducer/Converter, type 905 before being fed into the U.V. recorder.

Punch travel was recorded using an inductive type linear transducer manufactured by Vibrometer, type 8DT-10-250A. A maximum recording length of 220 mm was attainable using this transducer.

Pressure in the chamber was sensed by a Vibrometer high pressure quartz transducer, type 8QP-3000. This transducer has a maximum range of 300 MPa. The output from the transducer was fed into the U.V. recorder via a Vibro-meter charge amplifier, type TA-3/C.

Calibration curves appropriate to punch force, punch travel, blankholder force and chamber pressure are shown in Figures 4-7 respectively.

3.5 Choice of Materials

In choosing materials for the programme of work consideration was given to materials which were deep drawn by industry and which exhibited a range in those properties such as strength, ductility and work-hardening index.

70/30 brass was chosen as the standard material for comparison. It has a fairly high value of n , is ductile and is known to give fairly consistent results in conventional deep drawing. It is used extensively by industry for cartridge cases and tubes for subsequent

collapsing to form bellows for control valves.

Commercially pure aluminium S1C was chosen as being a fairly difficult material with which to achieve high values of drawing ratio. It lacks strength and has a low value of n . Its r value is also fairly low.

Typical uses include production of tubes for inks and paints.

Stainless steel is a material which is commonly deep drawn but often it is not possible to obtain the depth of draw required in a single operation. Therefore a number of operations with inter-stage annealing are required which significantly increases the manufacturing cost. The grade chosen was En58B, an 18/8 stainless steel.

The most commonly drawn quality of mild steel is CR1 (45), which is an aluminium stabilised steel as defined by BS 1449:1972 part 1. For the purpose of this programme of work, quality CR4 was chosen, a less formable rimming steel which has an r value greater than 1 but a significantly smaller value of n compared to either 70/30 brass or the En58B stainless steel used.

Photo micrographs of the four materials tested are shown in Figures 9-12 inclusive.

CHAPTER 4

EXPERIMENTAL PROCEDURE

4.1 Preliminary Tests

4.1.1 True Tensile Stress - Natural Tensile Strain Relationship

A number of tests were performed on specimens of each material to produce the true tensile stress - natural tensile strain curves up to the point of instability in uniaxial tension. The 70/30 brass, En58B stainless steel, and the CR4 mild steel were all tested in the "as received" condition. The commercially pure aluminium S1C was annealed by heating to a temperature of 350°C in a muffle furnace and allowing it to cool from this temperature in the furnace.

A number of tensile test specimens were cut from the sheet materials supplied in each of three directions; at 90° to the rolling direction, at 45° to the rolling direction and in the rolling direction. A centrally disposed 50 mm gauge length was marked on each of the specimens and this length divided into four equal parts using a vernier height gauge. Care was taken not to score the surface of the specimen too deeply and hence create a localised area of stress which would lead to premature failure. In determining the true tensile stress-natural tensile strain characteristic and the material properties the experimental values were averaged over three specimens in each direction.

The characteristic curves for these materials are presented in Figure 13 to Figure 16 inclusive and the material properties are listed in Table 1.

4.1.2 Blankholder Force

For each material the appropriate blankholder force was determined. The force adopted was the minimum necessary to prevent wrinkling of the largest blank. Thereafter this value was maintained constant for a given material by the use of a torque wrench. The establishment of the blankholder force for a given torque setting is presented in Appendix 2. The values of blankholder force adopted in the work are presented in Table 2.

4.1.3 Choice of Lubricant

A number of lubricants were tested during conventional drawing to obtain an optimum lubricant for the materials and experimental conditions prevailing in the work.

Advice was also taken from the oil and pump suppliers since in addition the lubricant had to be compatible, with the oil used during the hydrostatic deep drawing programme.

Throughout the work zinc stearate was used in a 50% by volume base of stabilised trichloroethane. This had been successfully employed by Slater during undergraduate deep drawing experiments at The City University and was capable of being easily applied by brush. To maintain consistency, fresh lubricant was mixed for each attempt at a drawing operation.

4.2 Main Programme of Work

4.2.1 Friction Around the Punch Profile

Strips of each of the materials, 50 mm wide by 500 mm long were cut from the metal sheets in the direction of rolling.* Care was taken to ensure that the edges were not damaged and thus provide a potential source of premature failure. The strips were elongated in the Hounsfield tensometer using the tooling previously described. Extensions in the short leg were measured using the extensometer whilst those in the long leg were measured using a toolmaker's microscope. (see Figure 8b)

Recordings were taken at intervals of 0.25 kN until fracture of the specimen took place. As a check on the material properties a load-extension graph was automatically recorded.

The procedure was carried out using both the plain rollers and the knurled rollers. Two further tests were carried out, firstly terminating the test at a low value of natural strain and secondly at about half the value of natural strain at which fracture occurred during the original test.

The results were used to plot graphs of true tensile stress in the long leg against natural tensile strain in the long leg, and natural tensile strain in the short leg against natural tensile strain in the long leg, both for the plain rollers and for the knurled rollers.

The graphs were used to obtain a value for the load in

* these strips were reduced to 12 mm over the middle 400 mm.

each of the legs of the specimen and a coefficient of friction calculated using the theoretical analysis presented in Chapter 5.

The results are shown plotted in Figure 17 to Figure 24 inclusive and the variation of the coefficients of friction with respect to natural tensile strain in the long leg are shown in Figure 25 to Figure 32 inclusive. The surface of the material in the vicinity of the punch profile radius was investigated under a scanning electron microscope and the results are shown in Figure 33 to Figure 40 inclusive.

4.2.2 Preparation of Blanks for Deep Drawing

Blanks up to a diameter of 89 mm were obtained using existing tooling designed to produce blanks for undergraduate experiments. These blanks were subsequently turned to size to ensure accuracy and to remove the work-hardened edge. Larger blank sizes were cut direct from the sheet and then turned to size as above. In the early stages of the drawing programme, increments of 0.5 mm diameter were taken for succeeding tests. However, as the limiting drawing ratio was approached these increments were reduced to 0.1 mm diameter. All blanks were carefully deburred, degreased and marked with a circular grid pattern using the Electromark process and a stencil producing 2.5 mm diameter circles.

4.2.3 Determination of Limiting Drawing Ratio for Conventional Drawing

The blanks were lubricated on the gridded side only, this face being accurately positioned on the die surface. The blankholder face was lubricated and tightened down onto the blank to the torque value shown in Table 2. Care was taken to ensure that no lubricant came into contact with the punch.

Using the smooth faced punch, drawing was initiated whilst recordings were taken of the punch force and punch travel, and the blankholder force. The procedure was repeated with increasing blank sizes until the limiting drawing ratio was reached. For the purposes of these investigations, a successful draw on two consecutive operations was considered satisfactory. Conversely, two consecutive failures were deemed as indicating that too high a drawing ratio had been attempted. The procedure was repeated using the knurled punch.

4.2.4 Determination of Limiting Drawing Ratio for Hydrostatically Assisted Deep Drawing

Initially, tests were carried out by drawing the blanks into the oil filled chamber without the oil being pressurised by the pump. The relief valve was set to 100 Pa and tests carried out with the knurled punch to attempt to increase the drawing ratio. The relief valve setting was progressively increased until no

further increase in the drawing ratio was obtained.

Subsequent tests were carried out by pre-pressurising the chamber prior to initiation of the draw. This was achieved by locating the blank as previously described and the punch brought down until it was in contact with the blank. The blankholder was tightened and the chamber pressurised using the pump. The value of this initial pressure was increased until no further increase in the drawing ratio could be obtained.

The criteria for success or failure of a particular blank diameter were taken to be the same as for the conventional drawing tests.

4.3 Data Recorded

In all cases the punch force, punch travel and the blankholder force were recorded. In the case of the pressure programme additional records of the initial chamber pressure and the final chamber pressure were recorded. In each case and for all materials the type of failure and the value of the drawing ratio at which failure occurred were recorded.

CHAPTER 5

THEORETICAL CONSIDERATIONS

5.1 Derivation of the Punch Force

The conditions of straining even in the simplest deep - drawing operation is extremely complex and does not lend itself to easy analysis. However, considerable work has been carried out with a view to determining the punch force required to draw a cylindrical cup.

Slater (38) summarises the punch force requirements as having to;

- (1) radial draw the flange,
- (2) overcome frictional resistance at the blankholder and flange interfaces,
- (3) bend and unbend the blank over the die profile with a tensile stress present,
- (4) radial draw over the die profile,
- (5) overcome frictional resistance at the die profile,
- and (6) bend and unbend the blank over the punch profile.

It is assumed that no relative movement occurs between the punch face and the blank. However, stretching and sliding over the punch head must occur to some extent, the amount depending upon the punch head geometry , particularly the profile radius, and the frictional conditions existing.

Also ignored by the analysis is the additional force required to stretch the blank material between the die

profile and the punch profile. However, with a correct choice of punch-die clearance, this component should be insignificant. Forces required to produce ironing are also neglected, this being justified as before, by the correct choice of punch-die clearance.

Using the above requirements the components of the punch force may be specified as follows;

1. radial stress to draw the whole flange;

$$\sigma_r = m\bar{Y}\ln(R_o/r_A) \quad (10)$$

2. stress required to overcome frictional resistance at the blankholder-flange and die-flange interfaces;

$$\sigma_{r(f)} = \mu F/\pi R_o t_o \quad (11)$$

3. stress required to bend the material over the die profile;

$$\sigma_B = \{\bar{Y}t_o/(2\sqrt{3}r_d)\}\{1+(\sigma_A/Y)^2\} \quad (12)$$

4. stress required to unbend the material from around the die profile;

$$\sigma_u = \{\bar{Y}t_o/(2\sqrt{3}r_d)\}\{1+(\sigma_B/Y)^2\} \quad (13)$$

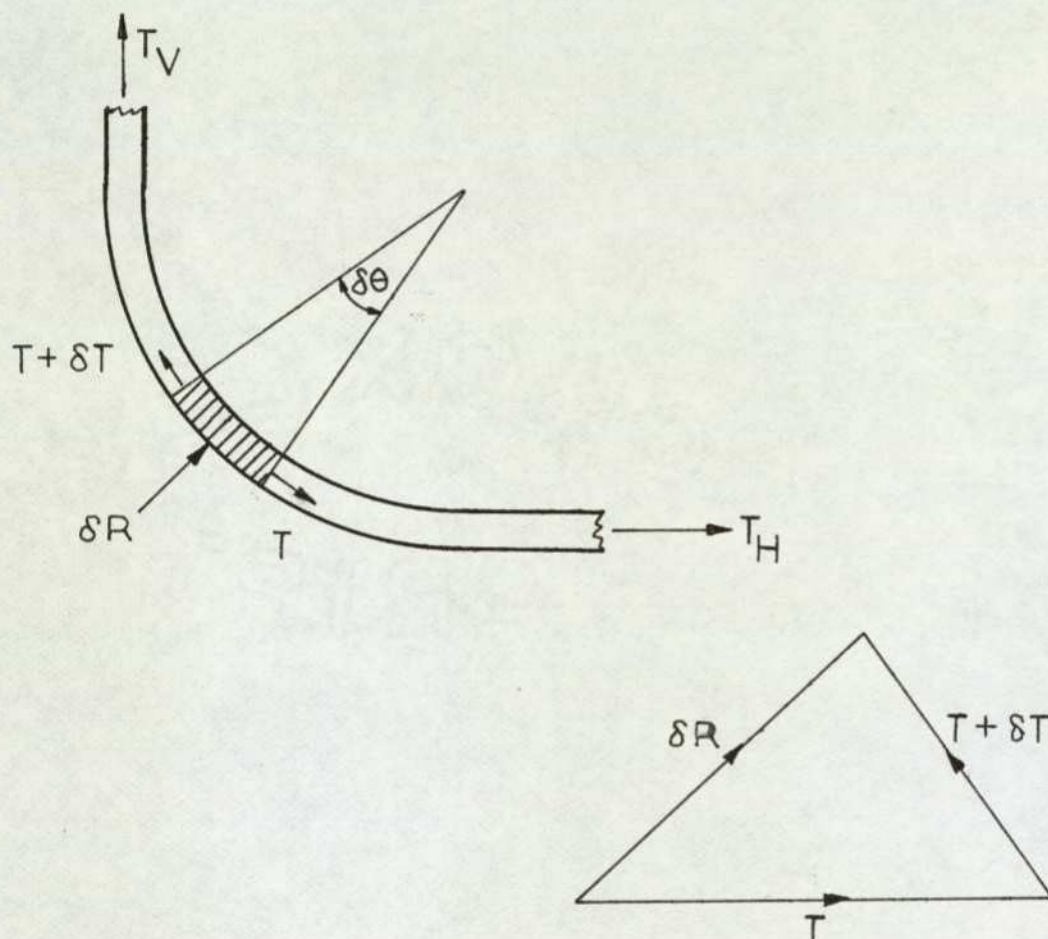
5. stress required to produce radial drawing over the die profile.

$$\sigma_{r(A-B)} = m\bar{Y}\ln(r_A/\bar{r}_p) \quad (14)$$

The complete derivation of the above equations can be found in Appendix 1.

5.2 Frictional Conditions at Blank-Punch Profile

The conditions existing at the interface between the blank and the punch profile may be investigated with reference to the following diagram;



When the material is pulled round the punch profile radius the coefficient of friction has a pronounced effect upon the tension in the cup wall, T_V , and the tension in the base of the cup, T_H .

If the tensions on the shaded element shown in the above diagram are T and $T + \delta T$, and the reaction of the

punch on the material is δR , then;

$$\delta T = \mu \delta R \quad (15)$$

$$\delta T = \mu T \delta \theta$$

as $\delta \theta \rightarrow 0$ then

$$\delta T/T = \mu \delta \theta \quad (16)$$

$$\int_{T_H}^{T_V} (dT/T) = \mu \int_0^\theta d\theta$$

$$\ln(T_V/T_H) = \mu \theta \quad (17)$$

$$T_V/T_H = \exp(\mu \theta)$$

$$\text{whence } \mu = \ln(T_V/T_H)/\theta \quad (18)$$

5.2.1 Limiting Condition

For any given material of tensile yield stress Y , then it is possible to determine the maximum strain possible for zero strain in the base of the cup.

$$\text{If } T_H < Y A_O$$

$$\text{and } T_V = Y_S A_O \text{ where } Y_S \text{ is the wall tensile stress}$$

$$\text{then } Y_S A_O / Y A_O < \exp(\mu \theta) \quad (19)$$

If μ is taken to be approximately 0.2 and $\theta = \pi/2$, then the relationship between the tensile stress in the wall of the cup and the tensile yield stress of the material is 1.37. For a yield stress of 1300 MPa the maximum tensile stress in the cup wall would be 1781 MPa or a punch force of 127 kN for stainless steel En58B.

CHAPTER 6

RESULTS AND DISCUSSION

6.1 Properties of the Materials Tested

6.1.1 70/30 Brass

The relationship between true tensile stress and tensile natural strain for this material is shown in Figure 13. The shape is characteristic for this type of material and tests for significance indicate that the results can be considered to be greater than 95% significant. From the results a relationship was derived as follows:

$$\sigma = 736\epsilon^{0.48} \text{ MPa} \quad (20)$$

The value of 0.48 for n is typical for this material and agrees with the findings of other workers (39,40). The value indicates that the material is fairly ductile and would be expected to draw well. The value of n found is only approximate however since n decreases as ϵ increases. Also the material can be expected to appear very ductile when deep drawn compared to its performance during the uniaxial test.

The material exhibited both planar and normal anisotropy as represented by a variation in strain ratio (r) of 0.21 and a mean strain ratio ($\bar{r}$) of 0.94. This pronounced effect of normal anisotropy compared to planar anisotropy is not uncommon and would predict the formation of ears at $\pm 45^\circ$ to the axes of anisotropy. The value of 0.94 for $\bar{r}$ being less than 1, would

indicate generally an unfavourable material for deep drawing.

6.1.2 Stainless Steel En58B

The relationship of true tensile stress against tensile natural strain is shown in Figure 14. The shape is typical for this grade of material and closely resembles that for 70/30 brass. Tests for significance on the results indicate greater than 95% significance. From the results a relationship was derived as follows:

$$\sigma = 1302\epsilon^{0.43} \text{ MPa} \quad (21)$$

The value of 0.43 for n, whilst not being as high as for the brass tested, shows a material which is likely to draw equally well. The same limitations apply to the accuracy of the value of n as for the brass.

Both planar and normal anisotropy were exhibited by the material with normal anisotropy more pronounced than the planar anisotropy. A value of 0.13 being obtained for planar anisotropy(r) and a value of 1.08 for normal anisotropy ($\bar{r}$).

A somewhat unusual result was obtained for the planar anisotropy with the strain ratio in the rolling direction being greater than the strain ratio at both 90° and 45° to the direction of rolling. A higher value of strain ratio in the direction at 90° to the rolling direction would have been expected so giving rise to a negative variation in strain ratio.

The value of 1.08 for $\bar{r}$ indicates a material with some resistance to thinning. With a value greater than unity, the flow stress in uniaxial tension is raised relative to the flow stress under a tensile-compressive system and hence the material over the punch head is strengthened relative to the material in the flange. A greater limiting drawing ratio could therefore be expected to be achieved.

6.1.3 Mild Steel CR4

The relationship between true tensile stress and tensile natural strain for the CR4 grade of mild steel is shown plotted in Figure 15. The shape of the curve shows a more gradual increase of true stress with natural strain than for either the 70/30 brass or the stainless steel En58B. Tests for significance on the results indicated greater than 99% significance. The relationship was computed to be:

$$\sigma = 664\epsilon^{0.25} \text{ MPa} \quad (22)$$

The value of 0.25 for n indicates a material more work-hardened than either of the two previous materials and hence unlikely to draw as well. This is not unexpected since the material is not the ideal steel for deep drawing, grade CR1 usually being chosen for this particular process.

Planar anisotropy was much more pronounced than for either of the two previous materials, a negative value for the variation in strain ratio being obtained.

However, the value of 1.12 for $\bar{r}$ indicates a material with some resistance to thinning and hence capable of being drawn to a greater reduction before failure.

6.1.4 Aluminium S1C

The relationship between true tensile stress and tensile natural strain for this material is shown in Figure 16. The relationship is more similar to the mild steel CR4 than to either the 70/30 brass or the stainless steel En58B, with a more gradual increase in stress at the lower values of natural strain. Tests for significance on the results indicated greater than 99% significance. The relationship is computed to be:

$$\sigma = 128\epsilon^{0.30} \text{ MPa} \quad (23)$$

The value of 0.30 for n was a little higher than expected when compared with other workers' results. However, this was probably indicative of the heat treatment carried out on the material prior to its being tested.

The material exhibited considerable planar anisotropy, far greater than any of the other materials tested. A negative value for the variation in strain ratio (r) of 0.62 was obtained showing a pronounced tendency to the formation of ears at 0° and 90° to the axes of anisotropy.

The value of 0.58 for $\bar{r}$ was the lowest value of mean strain ratio obtained indicating very little resistance

to thinning. Such a value would suggest a low value for the limiting drawing ratio, probably lower than 2. This was to be expected since values of drawing ratio of 1.7 to 1.9 are commonly quoted for soft aluminium.

6.2 Strain Relationship and Frictional Coefficient

6.2.1 Stainless Steel En58B

The graphs showing the relationship between strain in the short leg against strain in the long leg are shown in Figure 17 for the plain rollers and Figure 18 for the knurled rollers.

In both cases failure occurred in the long leg due to instability in tension at a natural strain of 0.3 in the long leg. However, the strain in the short leg at the point of failure was reduced by some 20% when using knurled rollers.

At the lower value of strain there was clear evidence with the knurled rollers of delay in the onset of strain in the short leg. Some evidence of this is apparent with plain rollers but it is not as pronounced. After a value of 0.08 strain in the long leg had been obtained strain in the short leg increased at a slower rate with the knurled rollers.

Graphs of coefficient of friction against strain in the long leg are shown in Figure 27 for plain rollers and in Figure 28 for knurled rollers. In both cases the graphs are shown dashed below a value of 0.05 strain since it is considered that values below this point

cannot be guaranteed to the same accuracy as those obtained at the higher values. However, the general trend is for an increase in the coefficient of friction from approximately 0.1 at low values of strain to a substantially constant value at a strain of 0.22 with the knurled rollers and 0.25 with the plain rollers. This constant value at 0.22 for the knurled rollers shows an increase of 69% over the value of 0.13 obtained with the plain rollers.

6.2.2 70/30 Brass

The graphs showing the relationship between strain in the short leg against strain in the long leg are shown in Figure 19 for the plain rollers and in Figure 20 for the knurled rollers.

In both cases failure occurred in the long leg due to instability in tension at a natural strain of 0.23 in the long leg. Using plain rollers the strain in the short leg at failure was 80% of the strain in the long leg. However, this value was reduced to only 30% of the strain in the long leg when using knurled rollers.

As evidenced by the graph of Figure 20, at low values of natural strain the rate of increase of strain in the short leg was considerably reduced using knurled rollers. After a natural strain of 0.05 with knurled rollers, and 0.02 with plain rollers, the strain in the short leg increased, but at a slower rate, with strain in the long leg.

Graphs of coefficient of friction against strain in the

long leg are shown in Figure 25 for plain rollers and Figure 26 for knurled rollers. In both cases the graphs are shown dashed below a strain of 0.05 since it is considered that values below this point cannot be guaranteed to the same degree of accuracy as those at higher values of strain. The two graphs show a fundamental difference at low values of strain. Using plain rollers the coefficient of friction is initially high, falling to a constant value of 0.07 from a strain of approximately 0.12 onwards. On the other hand when knurled rollers were used the coefficient of friction is initially low, rising to a constant value of 0.26 from a strain of 0.08 onwards. The coefficient of friction at this value is 0.26 showing a 271% increase over the value obtained with plain rollers.

6.2.3 Mild Steel CR4

The graphs showing the relationship between strain in the short leg against strain in the long leg are shown in Figure 21 for the plain rollers and in Figure 22 for the knurled rollers.

In both cases failure occurred in the long leg due to instability in tension at a natural strain of 0.24 in the long leg for plain rollers, and a strain of 0.3 in the long leg for knurled rollers. The strain in the short leg at failure was 18% of the strain in the long leg using plain rollers, and less than 1% of the strain in the long leg when using knurled rollers.

In both cases the strain in the short leg increased with strain in the long leg to a constant value at a strain in the long leg of approximately 0.2.

Graphs of coefficient of friction against strain in the long leg are shown in Figure 29 for plain rollers and in Figure 30 for knurled rollers. In both cases the graphs are shown dashed below a strain of 0.05 in the long leg since it is considered that values below this value cannot be guaranteed to the same accuracy as those obtained at larger strains.

The two graphs show a fundamental difference at low values of strain. Using plain rollers the coefficient of friction is initially high, falling to a constant value of 0.13 at a strain of approximately 0.16 onwards. Using knurled rollers the coefficient of friction is initially low, rising to a substantially constant value of 0.4 at a strain of approximately 0.16 onwards. The value of 0.4 for the coefficient of friction shows an increase of 130% over that obtained with plain rollers.

6.2.4 Aluminium S1C

The graphs showing the relationship between strain in the short leg against strain in the long leg are shown in Figure 23 for plain rollers and in Figure 24 for knurled rollers.

Failure occurred in both cases at the radius where the material left the roller on the long leg side. Failure thus took place under approximately plane strain conditions. Failure occurred at a strain in the long

leg of 0.17 with plain rollers and at a strain in the long leg of 0.23 with knurled rollers. Strain in the short leg at failure was 30% of the strain in the long leg with plain rollers and less than 1% of the strain in the long leg with knurled rollers.

At failure, strain in the short leg was still increasing with plain rollers, whilst using knurled rollers the strain in the short leg had assumed a constant value at a strain in the long leg of 0.16.

Graphs of coefficient of friction against strain in the long leg are shown in Figure 31 for plain rollers and in Figure 32 for knurled rollers. Both graphs are shown dashed below a strain of 0.05 in the long leg since it is considered that values below this figure cannot be guaranteed to the same accuracy as those obtained at larger strains. The trend of both graphs is for the coefficient of friction to increase to a substantially constant value at a strain in the long leg of 0.11.

With plain rollers there is an increase in the coefficient of friction to 0.12 whilst with knurled rollers the coefficient of friction increases to 0.62. This value represents an increase of 416% over the maximum value obtained using plain rollers.

6.3 Scanning Electron Micrographs

6.3.1 Stainless Steel En58B

The micrograph for plain rollers is shown in Figure 33 and in Figure 34 for knurled rollers.

From the results at the two magnifications the effect of the plain rollers was to generally level the surface of the specimen due to the localised stressing of the peaks of the material surface.

The granular nature of the surface is the original surface structure, the substantially overall flat portion having been levelled as previously described.

In the case of the knurled rollers a different pattern is clearly demonstrated. At x 500 magnification, grooves may be seen running across the specimen. Between the grooves particularly at the top left hand side of the micrograph the original surface is seen to be untouched. Looking into the grooves at x 1000 magnification there would appear to be no evidence of tearing away of the surface, although a small amount of debris in the form of shavings is apparent. Once again the untouched original surface between the grooves is visible at the middle left of the micrograph.

6.3.2 70/30 Brass

The micrograph using plain rollers is shown in Figure 35 and for knurled rollers in Figure 36. Using plain rollers the micrograph at x 100 magnification shows random areas of severe damage among untouched areas of the original surface. The damage appears to have directionality indicating movement between the two surfaces. At x 500 magnification the severe tearing that has taken place gives rise to a layered surface

beneath the untouched surface.

The effects of knurled rollers, Figure 36, shows a different structure entirely. At x 50 magnification the uniform pattern of damage caused by the knurling is clearly demonstrated. Once again the direction of movement is visible and the intermittent nature of this motion can be clearly seen, particularly at the bottom of the micrograph. A considerable amount of debris is present, both on the surface of the material and within the grooves.

At x 5000 magnification the nature of this debris is apparent. It appears to be finely rolled up shavings lying curled upon the surface of the specimen; similar in shape to those obtained by a fine metal cutting process such as shaping.

6.3.3 Mild Steel CR4

Figure 37 shows the results using plain rollers and Figure 38 the results using knurled rollers. Using plain rollers the surface has obviously been levelled down although this is not as clear as for the stainless steel En58B, probably due to the lack of an identifiable original surface. However, the surface is seen to be layered and a considerable amount of debris is visible. This debris is more clearly visible at x 2000 magnification and although considerably smaller than for the 70/30 brass shavings appear to be of a similar structure.

In the case of knurled rollers the pattern changes to one of a grooving nature. At x 500 magnification it is seen that the material is pushed ahead of an individual knurl thus developing a prow which increases in height. The bottom of the grooves is layered clearly due to the pointed nature of the knurl tearing through the surface. At x 2000 magnification small pockets can be seen. Of interest at this magnification are the lines running from top to bottom of the micrograph. The size, distribution and nature indicate that they are cavities from which harder inclusions have been totally removed by the tearing action of the knurl.

6.3.4 Aluminium S1C

Figure 39 shows the results using plain rollers and Figure 40 the results using knurled rollers. Using plain rollers the surface of the specimen is levelled down and at x 500 magnification there is evidence of tearing having taken place. At x 50 magnification a direct comparison between the stressed surface and the original surface may be made, this micrograph having been taken at the edge of the radius.

In the case of the knurled rollers there is a clear change in the mechanism of gripping. Grooving is now apparent and the material has been caused to flow in front of the knurls. Although the micrographs cannot be used to gain a quantitative assessment of the depth of penetration, it can be seen that compared to the harder materials, mild steel CR4 and stainless steel En58B,

the depth of the penetration is considerable.

At the higher magnification, x 450, the layered nature of the bottom of the groove is evident as is at least one piece of material about to break off and become debris.

6.4 Conventional and Hydrostatically Assisted Drawing

6.4.1 Effects on LDR

Table 6 summarises the results obtained from the conventional and hydrostatically assisted deep drawing programme. The most significant results were obtained with the stainless steel En58B where the LDR was raised from 2.15 by conventional drawing to 3.0 using hydrostatically assisted drawing; with 70/30 brass the LDR was raised from 2.1 by conventional drawing to 2.96 by hydrostatically assisted drawing. Only marginal improvement was achieved with mild steel CR4 and no improvement at all was recorded with the aluminium S1C.

Figure 41 and Figure 42 show the variation in LDR achieved using stainless steel En58B and 70/30 brass. In both cases further attempts to increase the drawing ratio with pressure assistance resulted in failure of a partially drawn cup, high up in the wall of the cup. Previous failures using conventional drawing techniques, both with and without a knurled punch resulted in failure at the other end of the cup adjacent to the top of the punch profile radius.

6.4.2 Punch Force

Figure 43 and Figure 44 show the punch force recorded for both stainless steel En58B and 70/30 brass as a function of the limiting drawing ratio. The theoretical values calculated from equation A24 and A27 in Appendix 1 are shown plotted on the same axes for the purpose of comparison.

Scanning electron micrographs of the bottom of the successful and failed cups are shown in Figures 45 to 48 inclusive.

6.5 Discussion

As stated in Chapter 1 and Chapter 2, the failure of conventionally drawn cups is usually by instability in plane strain tension low down in the cup wall where thinning of the blank has taken place. In order to reduce this thinning, movement of the blank in the vicinity of the punch profile radius must be arrested. The results obtained by the previously described programme of work clearly provide a further understanding of the mechanism by which this is brought about using roughened punches and pressure assistance to clamp the blank onto the punch head and around the profile radius.

6.5.1 Effects of Punch Roughness and Material Properties

The results obtained by the programme of tensile testing of the materials, as illustrated by Figures 17 to 32 inclusive indicate a relationship between tensile natural

strain being applied and the coefficient of friction between the material and roller interface. This relationship is not purely strain dependent, but depends upon the surface characteristics of the material forming the profile radius and of the properties and surface characteristics of the material being strained. The surface of any material cannot be optically flat, it consists of peaks and valleys which can generally be described as "gently undulating" rather than pointed, although sharp points do exist. When two such materials are brought together, local contact pressures are extremely high resulting in collapse of the peaks of the material having the lower initial yield strength and possibly local pressure welding. Any subsequent force tending to cause relative movement between the two surfaces results in fracture of any local welded areas and the generation of local heat. The result of such fracture would be to remove material from the surface of that metal having the lower yield strength. As additional pressure is applied further collapse of the peaks takes place resulting in a continual increase in the area of contact. Whilst the frictional restraint is low enough to allow relative movement between the surfaces to proceed the pattern described is repeated, giving rise to the phenomenon known as "stick slip." However the scale of this effect is not the same for all materials (46,47) and there is not a general model available.

6.5.2 Tests with Plain Rollers

With reference to Figures 17, 19, 21 and 23 it can be seen that with all materials save the aluminium S1C the natural strain in the short leg proceeds at a slower rate than the natural strain in the long leg up to a natural strain in the long leg of approximately 0.05. This can be considered to be a penetration stage, during which time the metal is being formed around the profile of the roller. Collapse of the peaks takes place during this time and in the case of the aluminium S1C the metal is softer than any of the others tested and therefore a large contact area is attained shortly after the beginning of the test.

These results are borne out by the scanning electron micrographs, Figure 33 to Figure 40 inclusive, which clearly show that the degree of penetration changes, depending upon the material properties. In the case of stainless steel En58B, Figure 33, only a levelling down of the sheet irregularities is achieved since the material is hard and is covered by an oxide film which is likely to inhibit pressure welding between the material and the roller. The high 'n' value of the stainless steel also contributes to a lack of deformation. However, in the case of 70/30 brass, Figure 35, complete collapse of the peaks can be seen and this has led to local welding and subsequently to micro-fracture of the welded areas. Metal particles transferred as debris lead to further grooving and could assist in

the increase in the value of the coefficient of friction.

The relationship between the coefficient of friction at the material/punch interface and the natural strain in the long leg is an interesting one. In the following discussion this coefficient of friction will be referred to by its generally accepted designatory character, μ .

In the case of the stainless steel En58B and the aluminium S1C, μ increases with natural strain in the long leg whilst with 70/30 brass and mild steel CR4 it decreases. Each of the four materials eventually show a constant value of μ which is different for each material. Both the stainless steel En58B and the aluminium S1C are covered by an oxide film which is hard and is known to provide resistance to pressure welding at low values of strain. This would account for the lower value of μ observed at the start of straining with these metals. In the case of the stainless steel En58B, the good surface quality of the sheet, see Table 3, reduces the incidence of mechanical locking between the metal and the surface of the roller. This also has the effect of producing a lower value for μ .

However with the 70/30 brass and the mild steel CR4, no such oxide film is present and the initial values of μ are higher than for the stainless steel En58B and the aluminium S1C. The higher initial value may also be accounted for by the poorer surface qualities, see Table 3, of the 70/30 brass and the mild steel CR4.

As straining proceeds only a slight increase in the value of μ is obtained for the stainless steel En58B. The high initial yield strength of the material and the high value of its work-hardening index results in only minor collapse of the peaks. With the aluminium S1C however, its low hardness value results in ploughing of the surface, Figure 39, and an increase in μ at the material/roller interface.

In contrast, the 70/30 brass although more easily deformed work-hardens rapidly which appears to result in a more rapidly falling value of μ . Tearing of the surface, where stick-slip has occurred, is clearly visible in Figure 35. In the case of the mild steel CR4, which shows less tendency to work-harden, the fall in the value of μ is not so great. The surface asperities of the roller pull through the surface of the metal.

6.5.3 Tests with Knurled Rollers

When a knurled roller is used this initial value of μ appears less dependent upon the surface characteristics of the material and rather more on the ability of the knurls to penetrate the material. Thus the hardness of the material becomes the dominant factor. Before any penetration has taken place it might be expected that one of the results of using a knurled roller would be to reduce the overall area of contact, resulting in a smaller value for the coefficient of friction than obtained with plain rollers. This is generally

observed within the experimental limits save for the case of the aluminium S1C where the lower strength of the material offers little resistance to penetration of the sheet by the knurling. Continued straining in all cases results in an increase in the value of coefficient of friction to a substantially constant value for each material. In all cases the final value of μ achieved exceeds that obtained with plain rollers on the same material. It should be noted that the scanning electron micrographs clearly indicate that the interaction between the knurling and the sheet by which the coefficient of friction is increased varies from one material to another. With the aluminium S1C there is no evidence of stick-slip occurring, rather the knurls pull through the material ploughing out a groove. Material ploughed off forms up ahead of the knurl producing a prow. The subsequent force required to displace the metal from the front of the knurl is less than that required to shear junctions of intimate contact, hence the absence of stick-slip. This ploughing effect is noted in the scanning electron micrographs for 70/30 brass in Figure 36 and for the aluminium S1C in Figure 40 respectively. With the harder materials there is less piling up of the metal and a different form of grooving occurs as shown in Figure 34 for the stainless steel En58B and in Figure 38 for the mild steel CR4 respectively. The hardness of these materials reduces the depth of penetration and subsequently the knurls ride out of the grooves.

6.5.4 Conventional and Hydrostatically Assisted Drawing

Previous workers have only compared the two processes using 70/30 brass as the work material. The results reported here are similar in their general trends to those obtained by previous workers (43,44), but do not show such a marked improvement in LDR when using hydrostatically assisted drawing. The use of knurled punches in themselves produced a significant increase in the LDR not previously reported.

The extension of the work to include stainless steel En58B and the mild steel CR4, confirms the work on 70/30 brass, although the removal of plane strain failure in the cup wall was not achieved with the mild steel CR4.

For both the 70/30 brass and the stainless steel En58B, transfer of the mode of failure from one of plane strain tension in the base of the cup at the punch profile radius to one of "uniaxial tension" near to the die profile radius was achieved. Clearly this demonstrates that the use of the hydrostatic pressure together with a knurled punch can prevent thinning of the material due to movement around the punch profile.

With aluminium S1C however, no improvement was observed with the knurled punch, alone or with a hydrostatic pressure. This is initially surprising as this low 'n' value material, with its tendency to concentrate rather than to dissipate localisation, would be expected to show a greater response to an external strain equalising

method. That this did not occur is probably due to the softness of the material which permitted greater penetration of the knurling and hence the creation of highly stressed regions and effectively reducing the load supporting section. Due to its low 'n' value there was no compensatory increase in strength. Also the poor surface quality of the sheet material could imply incipient cracks. There is some evidence (50), to show that with a reduction in the severity of the knurling, an increase in the LDR may be obtained for this material.

6.5.5 Prevention of Failure

The results of the work carried out by pulling sheet metal strips around the rollers and by drawing conventionally and with hydrostatic assistance provides evidence of the way in which failure can be prevented. There appears to be three mechanisms which either separately or together contribute to the prevention of failure in deep drawing. These are:

- (a) the natural properties of the materials;
i.e. the 'r' value which provides a measure of the resistance to thinning and the 'n' value which provides a measure of the ability of the material to equilibrate the strain.
- (b) the 'binding' of the material to the punch by a mechanism of micro-welding due to the pressure at the interfaces of punch and material.

(c) mechanical locking of the material onto the head of the punch which depends upon the depth of penetration possible and the work-hardening characteristic of the material which affects the ability of the surface asperities of the punch to subsequently 'ride over' the prow formed by grooving.

With plain punches, (a) and (b) predominate whilst with knurled rollers (c) dominates.

Applying these observations and predictions to the deep drawing results, the following observations may be made;

With the 70/30 brass, Figure 45, considerable difference is apparent between the failed cups and the successful cups. The lower initial hardness of the 70/30 brass compared to the stainless steel En58B, allows penetration of the knurls on the punch. In the case of the cup which fractured the degree of strain was too large and the knurls were not able to stop the material from moving around the punch head. The application of the pressure ensures greater penetration of the knurls and this is clearly seen in Figure 45(b). In both cases work-hardening of the material will have occurred but only in the pressure process have the knurls held and not pulled through the surface of the material. The deeper penetration of the material under the conventional process with a knurled punch means that the LDR achieved was greater than for the stainless steel En58B.

However, since there is a maximum to the depth that the knurls can penetrate the increase in the LDR obtained for the 70/30 brass will not be as large as for the stainless steel En58B.

The small increase in the LDR obtained with a knurled punch on the stainless steel En58B is consistent. The initial hardness of the material plus the oxide layer results in little mechanical keying and practically no micro-welding. The application of the hydrostatic pressure increases the keying effect and is likely to break up the oxide layer allowing micro-welding to occur. The already high yield strength is increased by this mechanical working and a substantial increase in LDR results. The penetration of the knurls and the broken surface surrounded by the untouched original surface is clearly seen in Figure 46.

In both the 70/30 brass and the stainless steel En58B, the mechanical locking eventually breaks down when the knurls ride out of the grooves formed.

In the case of the mild steel CR4, penetration with the knurled punch is easier than with the stainless steel En58B, but unlike the 70/30 brass full depth of penetration is unlikely to be achieved with the application of the hydrostatic pressure. An increase in the LDR is to be expected but since the material does not appreciably work-harden the knurls pull through the surface of the material, Figure 47(a), restricting this increase.

The application of the hydrostatic pressure forces the knurls deeper but due to the small amount of springback which occurs stripping off the punch becomes a problem. In the programme of work carried out, the force required to strip the formed cup off the punch was greater than the capacity of the machine.

From the foregoing it may be anticipated that success with the aluminium S1C is extremely unlikely. Even without the application of the hydrostatic pressure the knurls bite deeply into the material locally reducing the wall section of the partly formed cup. This section is unable to support the drawing load which occurs quite early in the stroke. The severe penetration of the material and the damage caused is seen in Figure 48(a), for conventional drawing with a knurled punch and in Figure 48(b) when a hydrostatic pressure is applied.

However, it should be possible to achieve some success with the use of a rough punch providing the degree of roughness is carefully chosen to match the strength of the material.

6.5.6 Punch Force

An inspection of Figure 43 and Figure 44 for stainless steel En58B and 70/30 brass respectively, shows good correlation between the experimental values and the theoretical values of punch force. In the case of the stainless steel En58B, the experimental results are lower by approximately 9.5% for conventional drawing

and by approximately 7.5% for hydrostatically assisted drawing. For the 70/30 brass the experimental results are lower by approximately 7% for conventional drawing and by approximately 5.5% for hydrostatically assisted drawing.

The applied theory therefore provides a solution which will always be in excess of the practical results and this is of benefit to industry. At first sight it is not obvious as to why the accuracy should be better for the hydrostatically assisted drawing since the applied theory is only extended by the additional pressure acting over the base of the cup, see Appendix 1. However, the pressure readings were taken off the calibration graph, Figure 7, from the U.V. trace which could induce an error of ± 0.2 MPa. In the case of the stainless steel En58B this could result in an average additional error of 1% and in the case of the 70/30 brass an additional average error of approximately 1.5%.

CHAPTER 7

CONCLUSIONS AND RECOMMENDATIONS FOR FUTURE WORK

7.1 Conclusions

The conclusions drawn from this programme of work may be stated as follows;

1. Confirmation has been provided of the importance of the 'r' value and the 'n' value of the sheet metal on the subsequent success of the conventional deep drawing operation.
2. Increases in LDR may be obtained by the use of roughened punches when drawing 70/30 brass, mild steel CR4 and stainless steel En58B.
3. Increases in LDR over and above that obtained by a roughened punch may be obtained on 70/30 brass, mild steel CR4 and stainless steel EN58B, by the use of a hydrostatic pressure to clamp the blank onto the punch head and to deform it around the punch profile radius.
4. The magnitude of the increase in LDR is affected by the value of the work-hardening exponent 'n' increasing with it.

5. Attempts to obtain a significant increase in LDR by the use of roughened punches may be offset entirely by the surface roughness of the sheet material.

7.2 Recommendations for Future Work

1. The whole of this programme of work was carried out using two standard roller surfaces referred to as "plain" and "knurled." Work is required to be done on a number of other grades of finish in order to determine an optimum value of roughness for a given sheet material.
2. Complementary to the variation in roughness is the geometry of that roughness. The conventional knurl used in this work, whilst proving satisfactory for three of the materials, was unacceptable for the low strength aluminium S1C. A cone or hemisphere should be investigated to determine whether similar results could be obtained without the severe local stressing caused by the sharp point of the knurl.
3. Improvements obtained in the drawn cups using a hydrostatic pressure indicate that further reductions in material movement were achieved. Modifications to the testing equipment are required to enable a hydrostatic pressure to be applied to the specimen during the modified tensile test.

4. Whilst providing an extremely useful picture of the surface damage, the scanning electron microscope was unable to show the effects of the crystallographic structure. It is believed that transmission electron microscopy could provide this information.
5. All the tests carried out during this programme of work on friction were with specimens cut from sheet metal in the direction of rolling. Since the materials used and indeed most engineering materials exhibit variations in properties in the plane of the sheet, further study is required to provide complete information in a direction perpendicular to the direction of rolling.
6. To show the effects more clearly, specimens should be prepared to give a uniform surface characteristic prior to performing the tests. The work has demonstrated the importance of surface topography and following completion of the work and discussions with Pearce (49) the author intends to continue the work in this area.
7. In order to achieve greater accuracy of the results, particularly at the lower values of strain, a re-design of the apparatus is necessary to allow a modified specimen, with a reduced width in the short leg to be tested.

8. At the re-design stage considered at 7, thought should be given to the automatic recording of strain and force in both legs. This would reduce the considerable amount of manual calculation required and further enhance the accuracy of the results.
9. Since damage to the component affected by a knurled punch may not be acceptable, investigations of the effect on LDR of a smooth punch together with a hydrostatic pressure is likely to be worthwhile.
10. To further investigate the effect of the work-hardening index 'n', a programme should be undertaken with particular materials which have been successively cold worked to varying degrees. This could be of particular interest in the case of the aluminium SiC.

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APPENDIX 1

THEORY OF DEEP DRAWING

The established theory is related to the simplest deep drawing operation namely, the manufacture of a cylindrical cup from a circular flat blank of sheet metal. Slater (48) has produced a fairly rigorous treatment which takes into account most of the elements contributing to the punch force.

Considering Figure 1, the blank is divided up for the purposes of analysis into three identifiable zones.

A-B consisting of metal in contact with the die,
B-E consisting of metal which is not in contact
initially with either the punch or the die, and
E-F consisting of metal in contact with the head
of the punch

Subsequent movement of the punch imposes a radial stress on the material in the region A-B as it is drawn towards the die throat. Simultaneously a reduction in radii of this material induces a compressive stress resulting in a thickening of the material which would cause wrinkling if it were not prevented by the application of a blankholder force. Further movement of the material over the die profile results in plastic bending whilst the inner parts of the region C-D are further thinned by tension between the punch and the die. The nett effect for the outer parts of the region A-B is an increase in material thickness.

Part of the material in the region B-E is subjected to bending and sliding over the die profile, part to stretching in tension between the punch and the die and part to bending and sliding over the punch profile. Material in the region E-F is subject to stretching and sliding over the punch head ; the amount by which it is strained depends upon the geometry of the punch and the frictional conditions discussed in Chapter 1.

Pure radial drawing

Figure 1a indicates the physical conditions existing in a part of the flange. The radial stress σ_r is tensile (+ve) while p and σ_θ will be compressive (-ve).

Assuming a metal blankholder, very little thickening occurs in the flange and dt/dr is small ($\approx 2 \times 10^{-3}$), hence thickening is neglected in the following analysis.

Resolving forces on the element in the radial direction;

$$(\sigma_r + d\sigma_r)(r+dr)d\theta t - \sigma_r r d\theta t - 2\sigma_\theta \sin(d\theta/2) + 2\mu p r d\theta dr = 0 \quad (A1)$$

$$\text{Hence, } d\sigma_r r d\theta t + \sigma_r dr d\theta t - \sigma_\theta d\theta dr t + 2\mu p r d\theta dr = 0$$

Neglecting quantities of the third order and dividing by $r d\theta dr$;

$$t d\sigma_r/dr + \sigma_r t/r - \sigma_\theta t/r + 2\mu p = 0 \quad (A2)$$

$$\text{or } (d\sigma_r/dr)t + (\sigma_r - \sigma_\theta)t/r + 2\mu p = 0$$

For the frictionless case;

$$r d\sigma_r/dr + (\sigma_r - \sigma_\theta) = 0 \quad (A3)$$

The stresses σ_r and σ_θ must satisfy a yield criterion so that $\sigma_r - \sigma_\theta = m\bar{Y}$ known as the modified Tresca yield for a strain-hardening material. The coefficient m is obtained by the method of least squares as a compromise between the von Mises' and Tresca criteria.

$$\text{Thus } r d\sigma_r/dr + m\bar{Y} = 0 \quad (\text{A4})$$

$$\text{and } d\sigma_r = - m\bar{Y}dr/r$$

Assuming the stress-strain relationship for the material is given by:

$$\bar{\sigma} = A\bar{\epsilon}^n, \text{ where } \bar{\epsilon} \approx \epsilon_\theta = \ln(R/r), (\text{after Hill}), \text{ then,} \quad (\text{A5})$$

$$\bar{Y} = \int_0^{\bar{\epsilon}} \bar{\sigma} d\bar{\epsilon} / \int_0^{\bar{\epsilon}} d\bar{\epsilon}$$

$$\bar{Y} = A \int_0^{\bar{\epsilon}} d\bar{\epsilon} \cdot \bar{\epsilon}^n / \bar{\epsilon}$$

$$\text{that is ; } \bar{Y} = A\bar{\epsilon}^n/n+1 \quad (\text{A6})$$

Integration of the equation for σ_r gives the radial stress required at radius r to produce pure frictionless radial drawing.

$$\text{that is } \sigma_r = - m\bar{Y} \cdot \int_R^r dr/r$$

$$\sigma_r = m\bar{Y} \cdot \ln(R/r) \quad (\text{A7})$$

Frictional resistance of the flange

Frictional resistance takes place between the blank and the die surface and between the blank and the blankholder. The radial stress required to overcome this frictional resistance, for any given blankholder

force, F is therefore:

$$\sigma_{r(f)} = 2\mu F / 2\pi R_o t_o \quad \text{or}$$

$$\sigma_{r(f)} = \mu F / R_o t_o \quad (A8)$$

Plastic bending of material over the die profile

On reaching the die profile, elements of metal in the flat flange are formed into a quarter section of a hollow toroid at the point where the die profile commences. Subsequently they are unbent as the metal leaves the die profile into the straight portion of the cup wall.

Assuming that both bending and unbending occur instantaneously, then since the metal elements have no freedom to strain in the circumferential direction, the circumferential strain $\epsilon_\theta = 0$.

Figure 1b shows the distribution of stress and strain across the sheet when it is subjected to bending under tension due to the radial drawing stress where the die profile commences. It is assumed that plane sections remain plane during bending.

The distribution of strain across the sheet is, therefore, linear, the neutral surface of bending NN being displaced from the central surface by, say, an amount λ which can be calculated by equating the resultant force exerted on any transverse section to zero.

For the element shown let the applied tension at the ends due to radial drawing stress equal T per unit width.

The line of action of each tension is assumed normal to the terminal radial sections having an included angle $d\theta$. The radial components of these tensions can be considered balanced by the force due to a uniform compressive stress $(-p)$ at the inner surface of the sheet in contact with the die profile.

$$\text{Therefore, } 2T\sin(d\theta/2) - pad\theta = 0$$

$$\text{Since } d\theta \rightarrow 0, Td\theta = pad\theta \quad \text{or}$$

$$T = pa \tag{A9}$$

If the material is assumed to be strain-hardening, having a mean yield stress of $\bar{Y}$, and the drawing stress at the commencement of the die profile, σ_A so that;

$$\sigma_A/\bar{Y} = x, \text{ where } 0 < x < 1, \text{ then}$$

$$\bar{Y} \int_0^{(t/2)+\lambda} (T = x\bar{Y}t) - \bar{Y} \int_0^{(t/2)-\lambda} = 0$$

$$\text{Therefore, } \bar{Y}\{(t/2) + \lambda\} + x\bar{Y}t - \bar{Y}\{(t/2) - \lambda\} = 0$$

$$\text{Hence } (t/2) + \lambda + xt - (t/2) + \lambda = 0$$

$$\text{and } \lambda = xt/2 \tag{A10}$$

$$\text{From constancy of volume; } \epsilon_\theta + \epsilon_r + \epsilon_z = 0 \tag{A11}$$

$$\text{Since } \epsilon_\theta = 0, \text{ then } \epsilon_z = -\epsilon_r$$

The representative or equivalent strain is given by;

$$\bar{\epsilon} = \frac{\sqrt{2\{(\epsilon_r - \epsilon_\theta)^2 + (\epsilon_\theta - \epsilon_z)^2 + (\epsilon_z - \epsilon_r)^2\}}}{2} \tag{A12}$$

Putting in the values for ϵ_θ and ϵ_z above;

$$\bar{\epsilon} = \frac{\sqrt{2(\epsilon_r^2 + \epsilon_r^2 + 4\epsilon_r^2)}}{3}$$

$$\text{Therefore } \bar{\epsilon} = (2/\sqrt{3})\epsilon_r \quad (\text{A13})$$

$$\text{Similarly, } d\bar{\epsilon} = (2/\sqrt{3}) d\epsilon_r \quad (\text{A14})$$

The work done per unit width of sheet during bending of the element which is a distance y from the neutral surface is given by:

$dW = \text{stress} \times \text{incremental equivalent strain} \times \text{volume of the element.}$

$$dW = Y d\bar{\epsilon} (\rho + y) d\theta \cdot dy \quad (\text{A15})$$

$$= Y(2/\sqrt{3}) d\epsilon_r \cdot (\rho + y) d\theta$$

$$d\epsilon_r = \{(\rho + y)d\theta - \rho \cdot d\theta\} / (\rho + y)d\theta = y/(\rho + y)$$

$$\text{Therefore } dW = \bar{Y}(2/\sqrt{3}) y \cdot d\theta \cdot dy$$

The mean work per unit width during bending of the sheet over the die profile is given by:

$$W = (2/\sqrt{3}) \bar{Y} \cdot d\theta \left\{ \int_0^{(t/2)+\lambda} y dy + \int_0^{(t/2)-\lambda} y dy \right\} \quad \text{where } \lambda = (x/2)t$$

$$= (2/\sqrt{3}) \bar{Y} \cdot d\theta \{ (t/2 + \lambda)^2 + (t/2 - \lambda)^2 \} / 2$$

$$\text{Therefore } W = (2/\sqrt{3}) \bar{Y} \cdot d\theta (t^2/4)(1 + x^2) \quad (\text{A16})$$

Let the portion of the drawing stress which is required to produce the bending be σ_b , then the work done per

width by this stress operating through the mean distance,

$\rho_c d\theta = (a+t/2)d\theta$ is given by;

$$\sigma_b t(a+t/2)d\theta \quad \text{or} \quad (A17)$$

$$\sigma_b = \{\bar{Y}t/(2\sqrt{3})(a+t/2)\}(1+x^2) \quad (A18)$$

It is assumed that the thickness of the sheet remains constant and $t/2 \ll a$,

$$\text{Therefore, } \sigma_b \approx \{\bar{Y}t_o/2\sqrt{3}r_d\}\{1+(\sigma_B/Y)^2\} \quad (A19)$$

Radial drawing over the die profile

The increase in stress to produce radial drawing over the die profile from B-C is given by;

$$\sigma_{r(B \rightarrow C)} = m\bar{Y} \ln(r_B/\bar{r}_p) \quad (A20)$$

Frictional resistance at the die profile

The increase in stress required to overcome frictional resistance at the die profile from B-C, $\sigma_{f(B-C)}$, can be estimated by considering the blank as a belt passing over a pulley with Coulomb friction present at the interface.

If T_2 is the tension at C and T_1 is the tension at B,

$T_2/T_1 = \exp(\mu\theta)$ where θ is the angle of wrap $\approx \pi/2$.

For a small die profile radius, the cross sectional area of the blank at radius r_B and r_p are approximately equal,

Therefore, $\{(\sigma_B + \sigma_C) + \sigma_{f(B \rightarrow C)}\}/(\sigma_B + \sigma_b) \approx \exp(\mu\pi/2)$

$$\text{and } (\sigma_B + \sigma_C) + \sigma_{f(B \rightarrow C)} \approx (\sigma_B + \sigma_b) \exp(\mu\pi/2)$$

$$\text{and } \sigma_{f(B \rightarrow C)} \approx (\sigma_B + \sigma_b) \{ \exp(\mu\pi/2) - 1 \} \quad (\text{A21})$$

Unbending of the sheet at the die profile

Due to the different strain histories of the elements across the thickness of the sheet brought about by the bending operation, the analysis of unbending is complex. The outer fibres of the sheet are subjected to a greater strain than the inner layers to which is added the changes in strain due to radial drawing over the die profile. However, it is considered sufficiently accurate to use equation (A19), as a first approximation for the unbending stress σ_u , providing σ_C , the drawing stress before unbending is substituted for σ_B .

$$\text{Therefore } \sigma_u \approx \bar{Y} t_o / (2/3) r_d \{ 1 + (\sigma_C/Y)^2 \} \quad (\text{A22})$$

The drawing stress before unbending σ_C is given by:

$$\sigma_C = \sigma_{r(B)} + \sigma_{f(B)} + \sigma_b + \sigma_{r(B \rightarrow C)} + \sigma_{f(B \rightarrow C)} \quad (\text{A22})$$

Wall stress and punch force

The total wall stress in the drawn cup at radius $\bar{r}_p$ is

$$\sigma_w = \sigma_{r(B)} + \sigma_{f(B)} + \sigma_b + \sigma_{f(B \rightarrow C)} + \sigma_{r(B \rightarrow C)} + \sigma_u \quad (\text{A23})$$

$$\text{The punch force } P = \sigma_w 2\pi \bar{r}_p t_o \quad (\text{A24})$$

The frictionless drawing stress is given by:

$$\sigma_{r(h)} = m \bar{Y} \ln(R_o / \bar{r}_p) \quad (\text{A25})$$

Additional force due to hydrostatic pressure

There will be in addition to the above force an extra element due to the pressure of the hydraulic fluid when hydrostatically assisted drawing is being carried out. This will act over the head of the punch and over the base of the cup, the effective area is therefore;

$$\text{Area} = \pi(d + 2t_o)^2/4$$

If the pressure of the fluid is M then the additional force is given by;

$$M\pi(d + 2t_o)^2/4 \quad \text{and the total punch force;} \quad (\text{A26})$$

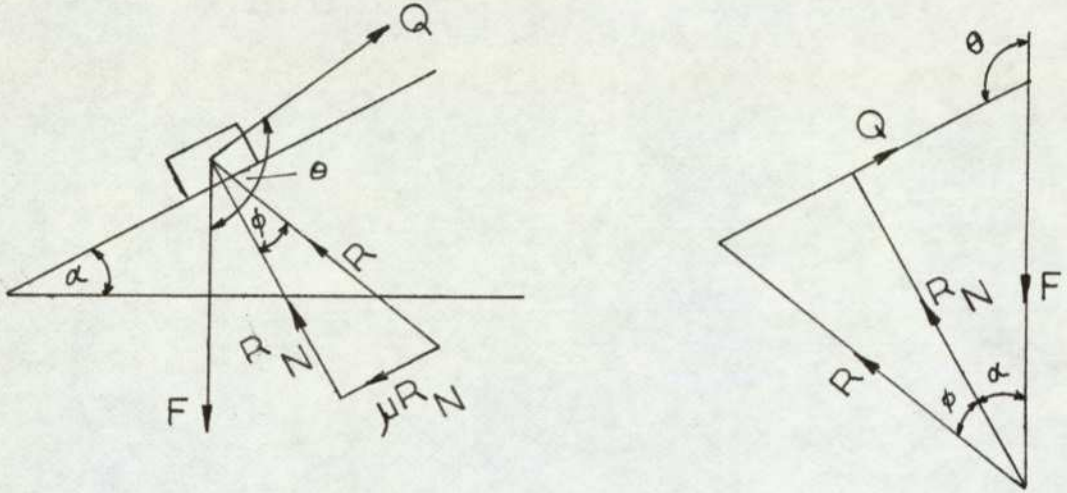
$$P = \sigma_w 2\pi \bar{r}_p t_o + M\pi(d + 2t_o)^2/4 \quad (\text{A27})$$

APPENDIX 2

ESTIMATION OF BLANKHOLDER FORCE (AFTER SLATER)

Motion up an inclined plane with Coulomb friction present

The hypothesis is represented below.



$$F/\sin\{180^{\circ}-(180^{\circ}-\theta)-(\alpha+\phi)\} = Q/\sin(\alpha+\phi) \quad (\text{A28})$$

$$F/\sin\{\theta-(\alpha+\phi)\} = Q/\sin(\alpha+\phi)$$

$$Q/F = \sin(\alpha+\phi)/\sin\{\theta-(\alpha+\phi)\} \quad (\text{A29})$$

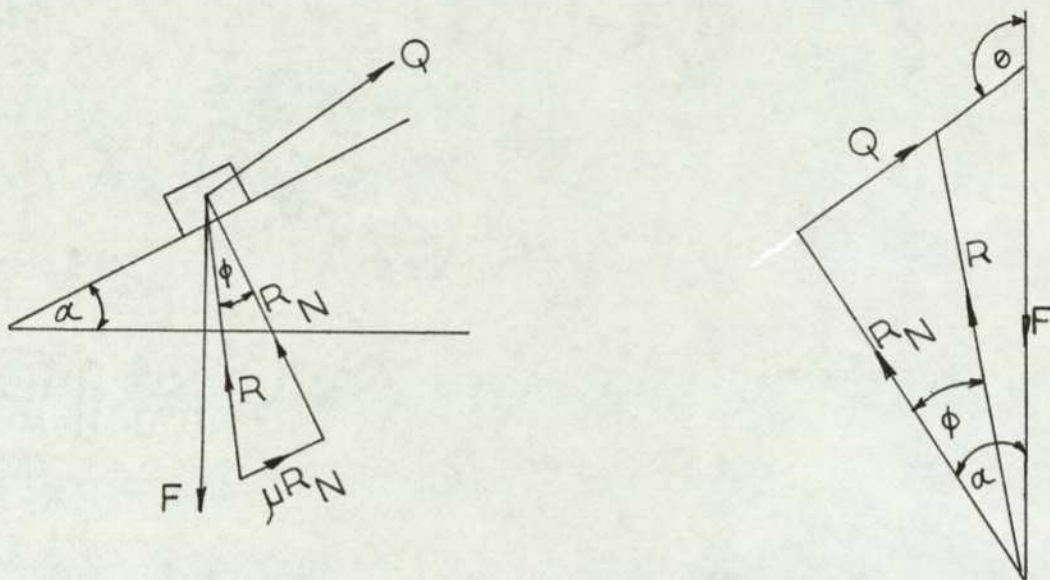
when $\theta = 90^{\circ}$, Q is applied horizontally, then

$$Q/F = \sin(\alpha+\phi)/\sin\{90^{\circ}-(\alpha+\phi)\}$$

$$Q/F = \sin(\alpha+\phi)/\cos(\alpha+\phi)$$

$$Q = F \tan(\alpha+\phi) \quad (\text{A30})$$

Motion down an inclined plane with Coulomb friction present



$$Q/\sin(\alpha-\phi) = F/\sin\{180^\circ-(180^\circ-\theta)-(\alpha-\phi)\} \quad (A31)$$

$$Q/F = \sin(\alpha-\phi)/\sin\{\theta-(\alpha-\phi)\}$$

when $\theta = 90^\circ$, Q is applied horizontally, then

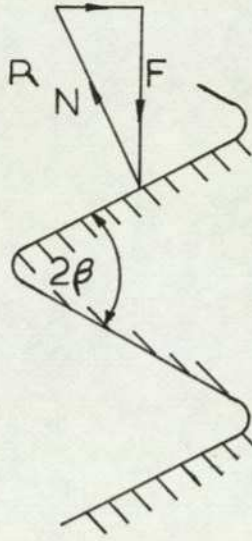
$$Q/F = \sin(\alpha-\phi)/\sin\{90^\circ-(\alpha-\phi)\}$$

$$Q/F = \sin(\alpha-\phi)/\cos(\alpha-\phi)$$

$$Q = F \tan(\alpha-\phi) \quad (A32)$$

Friction of Vee-threaded screws

Given a triangular screw thread section as shown on the following page, the normal reaction, R_N , at any point is inclined to the screw axis in two mutually perpendicular planes, due to the thread sloping in two directions.



However because the helix angle, $\alpha=\beta$, is usually small the following approximation is considered to be sufficiently accurate.

The axial component of the normal reaction must be equal to the axial force, then;

$$R_N \cos \beta = F, \text{ or } R_N = F / \cos \beta$$

$$R_N = F \sec \beta \tag{A33}$$

The friction resistance acts tangentially to the thread surfaces and is given by;

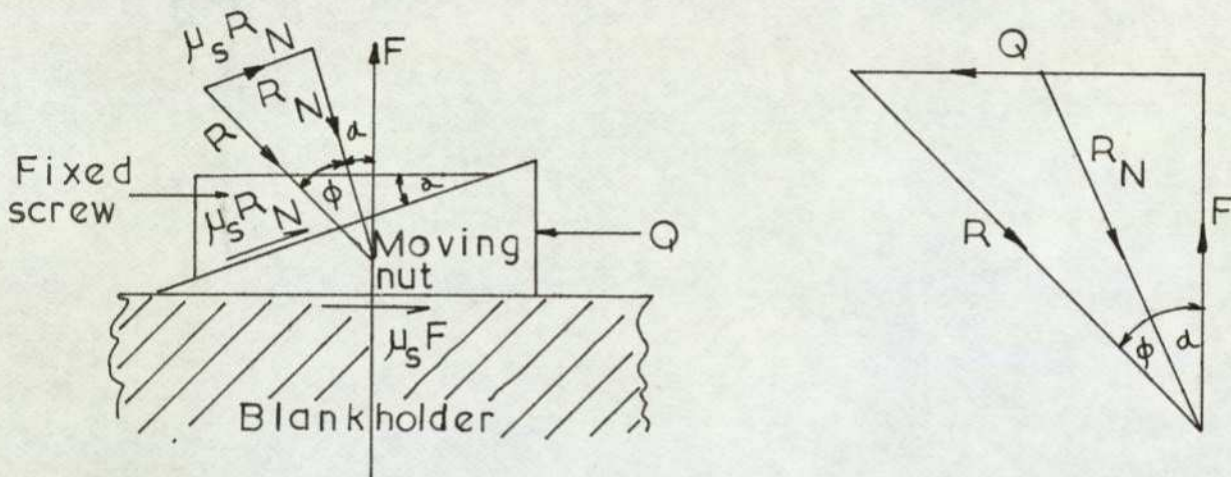
$$\mu R_N = \mu F \sec \beta = \mu_1 F \tag{A34}$$

where μ_1 can be regarded as the virtual coefficient

of friction, so that; $\phi_1 = \tan^{-1} \mu_1$ is the friction angle

Blankholder Force

The analysis above to determine the horizontal force required to cause uniform motion can be applied to the case of a nut applying a pressure to a blankholder.



If the nut is rotated so as to move the screw against the axial force F , then the screw is, in effect, moved up the inclined plane.

The torque applied at the nut is given by;

$$T = F(d/2)\tan(\alpha + \phi_1) \quad (A35)$$

In addition the torque must overcome the frictional force between the face of the nut and the blankholder flange, given by;

$$T_f = \mu_s F \bar{r} \quad (A36)$$

μ_s in the above equation is the coefficient of friction at the thread surfaces and the interface between the nut and the blankholder flange. $\bar{r}$ is the mean radius of the nut face in contact with the blankholder.

$$\text{Therefore; } T = F(d/2)\tan(\alpha+\phi) + \mu_s F\bar{r} \quad (\text{A37})$$

APPENDIX 3

SAMPLE CALCULATION OF PUNCH FORCE

The following example shows the method used in this work to determine the theoretical punch force required to draw a stainless steel En58B cup from a 90 mm diameter blank using the hydrostatically assisted technique.

The elements of the total punch force are determined with reference to Figure 1.

Determination of Mean Yield Stress ($\bar{Y}$)

Circumferential strain at C = $\ln(45/20)$

$$= 0.81$$

$$\begin{aligned} \text{Mean yield stress} &= \int_0^{\bar{\epsilon}} (\bar{\sigma} d\bar{\epsilon}) / \int_0^{\bar{\epsilon}} d\bar{\epsilon} \\ &= 1302 \int_0^{0.81} \epsilon^{0.43} d\epsilon \end{aligned}$$

$$\bar{Y} = 832.1 \text{ MPa}$$

Radial Stress at B, $\sigma_{r(B)}$ due to pure radial drawing

$$\begin{aligned} \sigma_{r(B)} &= m\bar{Y}\ln(R_o/r_B) \\ &= (1.1 \times 832.1)\ln(45/24) \end{aligned}$$

$$\sigma_{r(B)} = 575.3 \text{ MPa}$$

Radial Stress at B to overcome frictional resistance at the Blankholder-Flange and Die-Flange Interfaces, $\sigma_{f(B)}$

$$\sigma_{f(B)} = \mu F / \pi R_o t_o$$

From Table 2, F is 16 kN for stainless steel En58B.

μ is taken as 0.08 for the purpose of illustration.

$$\text{Therefore} \quad \sigma_{f(B)} = (0.08 \times 16 \times 10^3) / \pi \times 45 \times .59$$

$$\underline{\sigma_{f(B)} = 15.4 \text{ MPa}}$$

Radial Stress at B before bending over the die profile

$$\sigma_B = \sigma_{r(B)} + \sigma_{f(B)}$$

$$\underline{\sigma_B = 590.7 \text{ MPa}}$$

Stress to cause Bending over the Die Profile, σ_b

$$\begin{aligned} \sigma_b &= \{ \bar{Y} t_o / 2\sqrt{3} (r_d + t_o/2) \} [1 + (\sigma_B / \bar{Y})^2] \\ &= \{ 832.1 \times 0.59 / 2\sqrt{3} \} \times 4.295 \{ 1 + (590.1/832.1)^2 \} \end{aligned}$$

$$\underline{\sigma_b = 49.6 \text{ MPa}}$$

Increase in Stress Required to Produce Pure Radial Drawing over the Die Profile from B-C, $\sigma_{r(B-C)}$

$$\begin{aligned} \sigma_{r(B-C)} &= m \bar{Y} \ln(r_B / \bar{r}_p) \\ &= 1.1 \times 832.1 \ln(24/19.295) \end{aligned}$$

$$\underline{\sigma_{r(B-C)} = 199.7 \text{ MPa}}$$

Increase in Stress Required to Overcome Frictional Resistance at the Die Profile from B-C, $\sigma_{f(B-C)}$

$$\sigma_{f(B-C)} = (\sigma_B + \sigma_b) \{ \exp(\mu\pi/2) - 1 \}$$

$$= (590.7 + 49.7)\{\exp(0.08\pi/2)-1\}$$

$$\underline{\sigma_{f(B-C)} = 85.7 \text{ MPa}}$$

Total Stress at C before unbending, C

$$\sigma_c = \sigma_B + \sigma_b + \sigma_{r(B-C)} + \sigma_{F(B-C)}$$

$$= 590.7 + 49.6 + 199.7 + 85.7$$

$$\underline{\sigma_c = 925.8 \text{ MPa}}$$

Increase in Stress Required to Unbend the Sheet at C, σ_u

$$\sigma_u = \bar{Y} t_o / 2\sqrt{3} (r_d + t_o/2) \{1 + (\sigma_c / \bar{Y})^2\}$$

$$= \{832.1 \times 0.59 / 2\sqrt{3}\} \times 4.295 \{1 + (925.7/832.1)^2\}$$

$$\underline{\sigma_u = 73.8 \text{ MPa}}$$

Total Wall Stress, σ_w

$$\sigma_w = \sigma_c + \sigma_u$$

$$\sigma_w = 925.8 + 73.8$$

$$\underline{\sigma_w = 999.6 \text{ MPa}}$$

Punch Force due to Drawing, P_d

$$P_d = \sigma_w \times 2\pi \bar{r}_p t_o$$

$$= 999.6 + 2\pi \times 19.295 \times 0.59$$

$$\underline{P_d = 71.5 \text{ kN}}$$

Punch Force due to Fluid Pressure, P_f

The maximum pressure of 34.5 MPa acts over the area of the base of the cup, $(d + 2t_o)^2/4$.

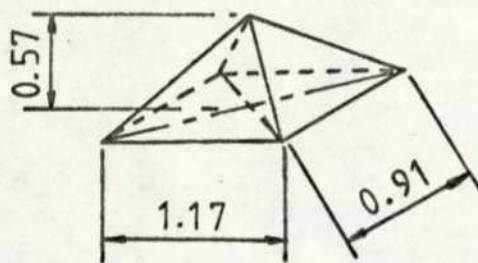
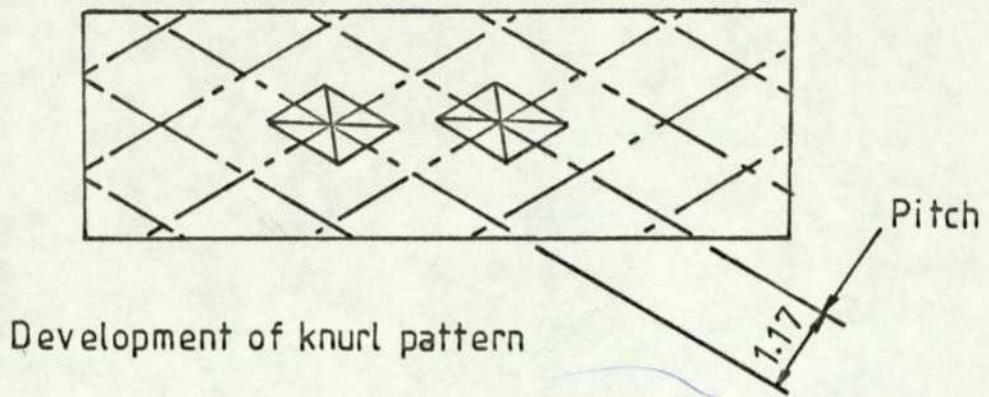
$$\text{Therefore } P_f = 34.5 \times \pi(39.18)^2/4$$

$$\underline{P_f = 41.56 \text{ kN}}$$

Total Theoretical Punch Force

$$\underline{P = 113.06 \text{ kN}}$$

APPENDIX 4 GEOMETRY OF KNURL PATTERN



Enlarged view of knurl

all dimensions in mm

MATERIAL	DEPTH OF PENETRATION mm	
	NO PRESSURE	WITH PRESSURE
70/30 BRASS	0.21	0.29
STAINLESS STEEL En 58 B	0.16	0.25
MILD STEEL CR4	0.19	0.31
ALUMINIUM S1C	0.25	0.36

Table showing degree of penetration

Table 1: Material Properties

Material	r_0	r_{45}	r_{90}	$\bar{r}$	A MPa	n
70/30 Brass	0.91	1.04	0.76	0.94	735	0.48
Stainless steel En58B	1.27	1.17	0.72	1.08	1302	0.43
Aluminium S1C	0.73	0.27	1.05	0.58	128	0.30
Mild steel CR4	1.23	0.95	1.33	1.12	664	0.25

Note! A and n are constants in the empirical equation

$$\sigma = A\epsilon^n$$

Table 2: Experimental Values of Blankholder Force

Material	Sheet Thickness mm	Blankholder Force kN
70/30 Brass	0.565	16
Stainless steel En58B	0.59	16
Aluminium S1C	0.57	3.4
Mild steel CR4	0.85	6.8

Note!
The values of Blankholder Force shown were
the minimum required to prevent wrinkling

Table 3: Initial Surface Roughness of Materials Used

Material	Average Surface Roughness Ra μm	
	With Grain	Against Grain
70/30 Brass	0,10	0,12
Stainless steel En 58B	0.05	0,06
Mild steel CR4	0,82	0,86
Aluminium S1C	0,17	0,32

Table 4: Vickers Hardness Numbers of Materials Used

Material	Hardness No.
70/30 Brass	82.4
Stainless steel En 58B	175
Mild steel CR4	125
Aluminium S1C	41.3

Table 5: Coefficient of Friction at the
Blank - Punch Profile Radius Interface

Material	Plain Punch		Knurled Punch	
	initial μ	final μ	initial μ	final μ
70/30 Brass	0.25	0.07	0.07	0.26
Stainless steel En58B	0.10	0.12	0.13	0.22
Aluminium S1C	0.05	0.12	0.30	0.62
Mild steel CR4	0.25	0.13	0.13	0.40

Table 6: Limiting Drawing Ratios

Material	Conventional Drawing	Knurled Punch	Hydrostatically Assisted Drawing
70/30 Brass	2.1	2.25	2.96
Stainless steel En58B	2.15	2.21	3.00
Mild steel CR4	1.9	2.0	2.18
Aluminium S1C	1.97	No improvement	No improvement

Table 7: Theoretical and Experimental Values
of Punch Force for Stainless Steel En58B

Drawing Ratio	Punch Force (kN)	
	Theoretical	Experimental
1.5	26.67	24.00
2	52.29	44.75
2.15	60.38	54.75
2.21	63.43	57.00
2.37	113.06	102.75
2.63	150.26	140.00
2.89	163.88	149.50
2.97	168.82	153.50

Table 8: Theoretical and Experimental
Values of Punch Force for 70/30 Brass

Drawing Ratio	Punch Force (kN)	
	Theoretical	Experimental
1.5	14	13
2	27.17	25.25
2.1	30.00	28
2.25	33.99	32
2.5	65.53	63.25
2.76	72.30	68
2.96	77.24	72.50

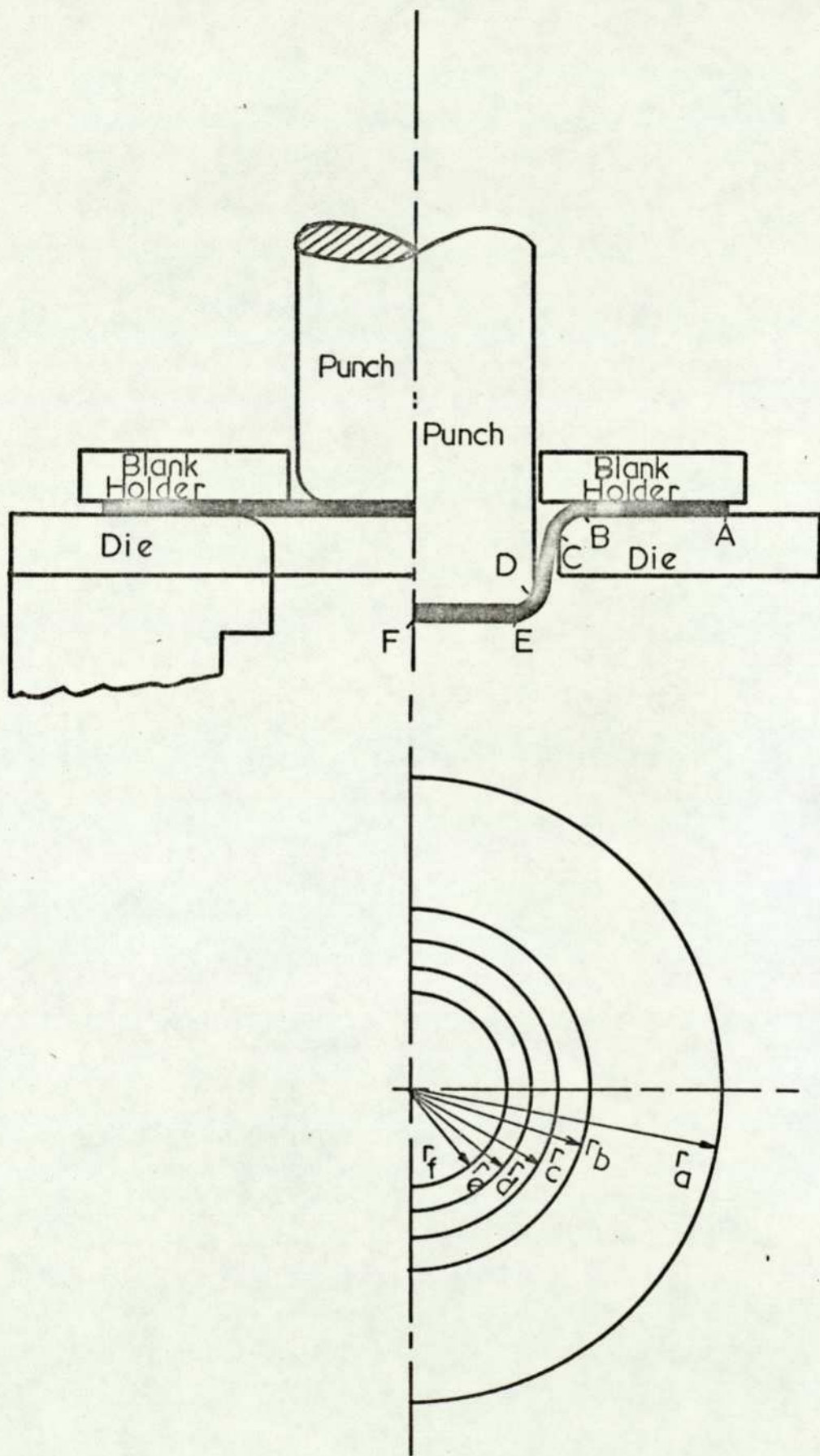


FIGURE I: FIRST STAGE DRAWING OF A CUP

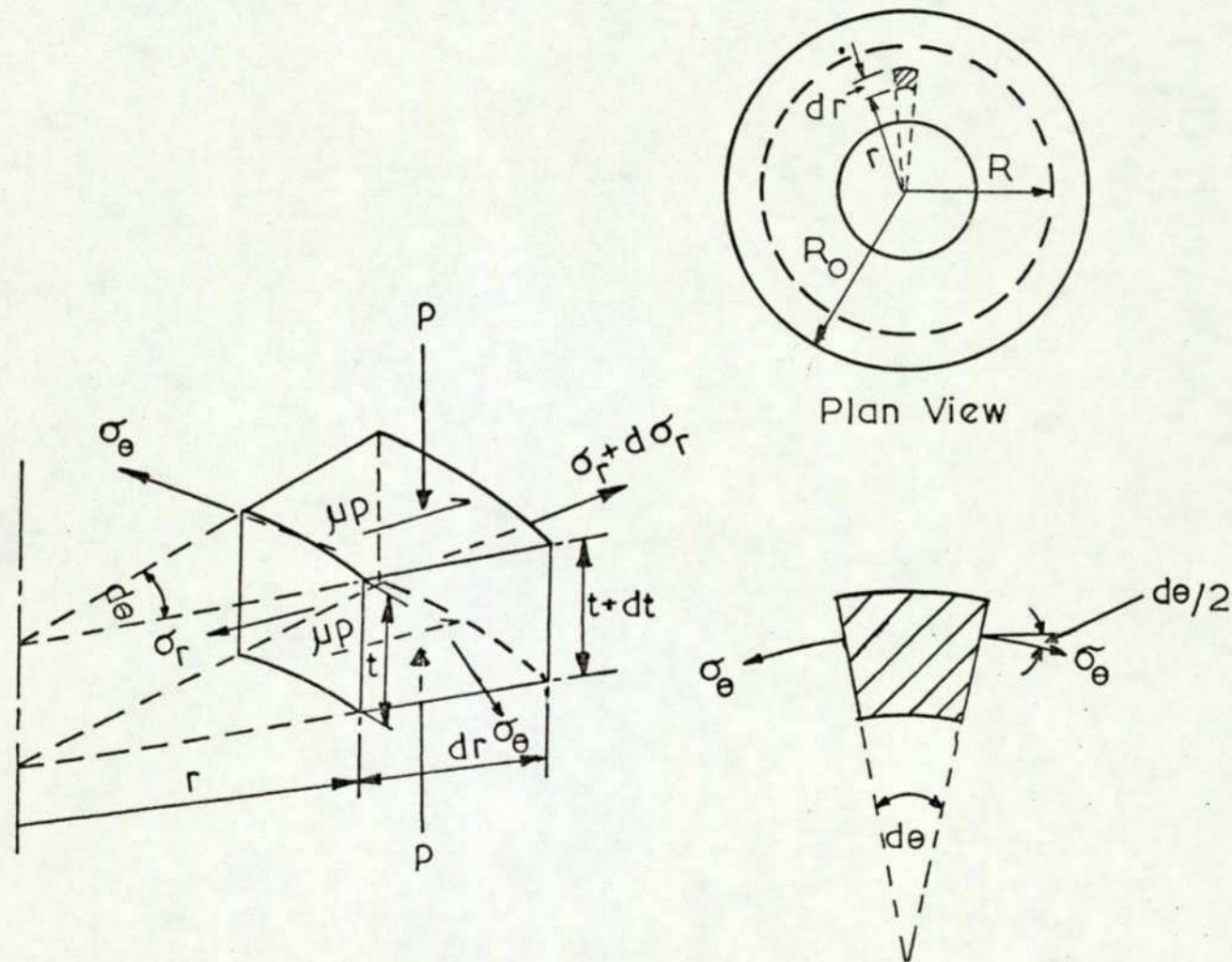


FIGURE 1a STRESSES ACTING ON AN ELEMENT IN THE FLANGE

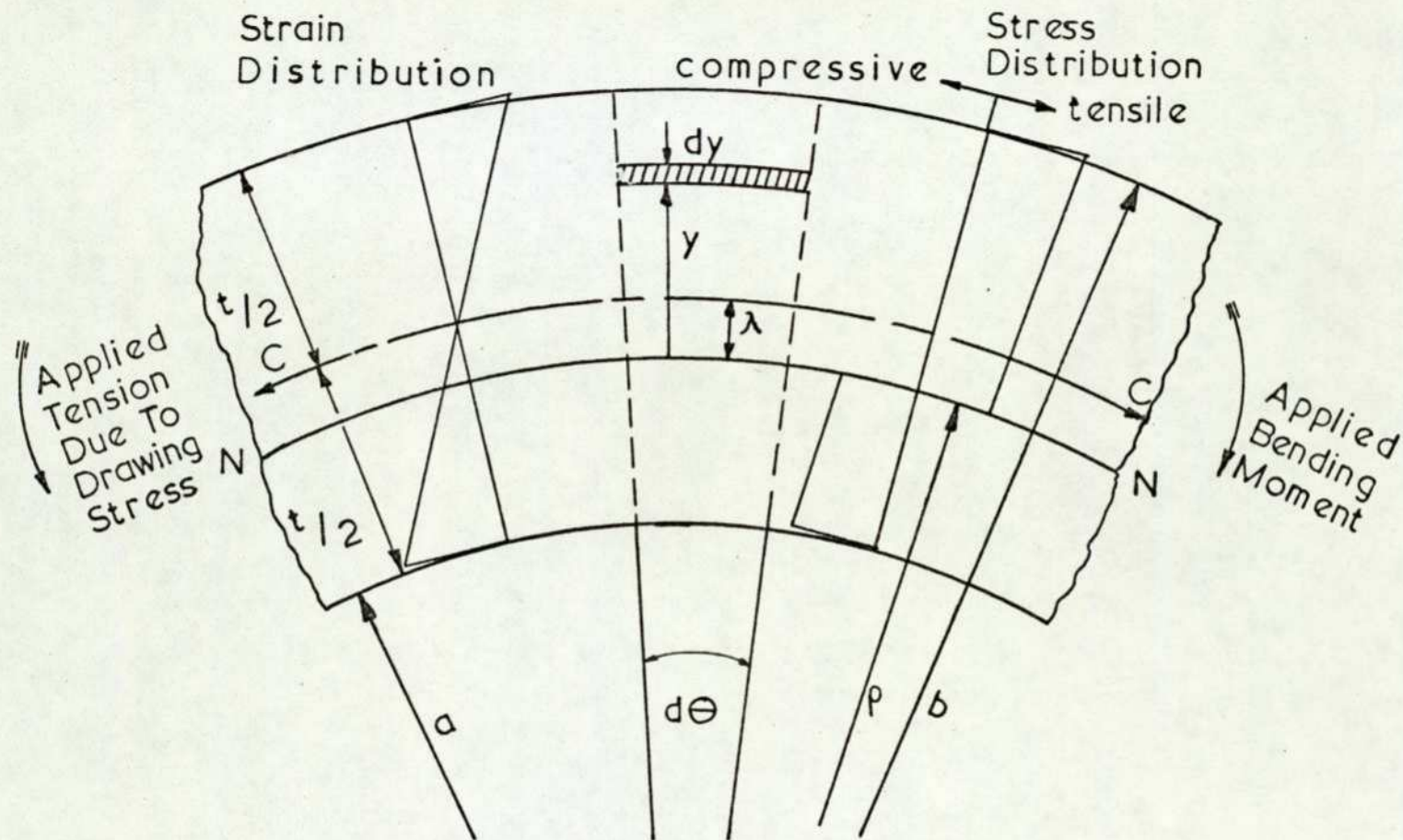


FIGURE 1b STRESS AND STRAIN DISTRIBUTION DURING BENDING UNDER TENSION AT THE DIE PROFILE



FIGURE 2: VIEW SHOWING SEAL IN DIE FACE

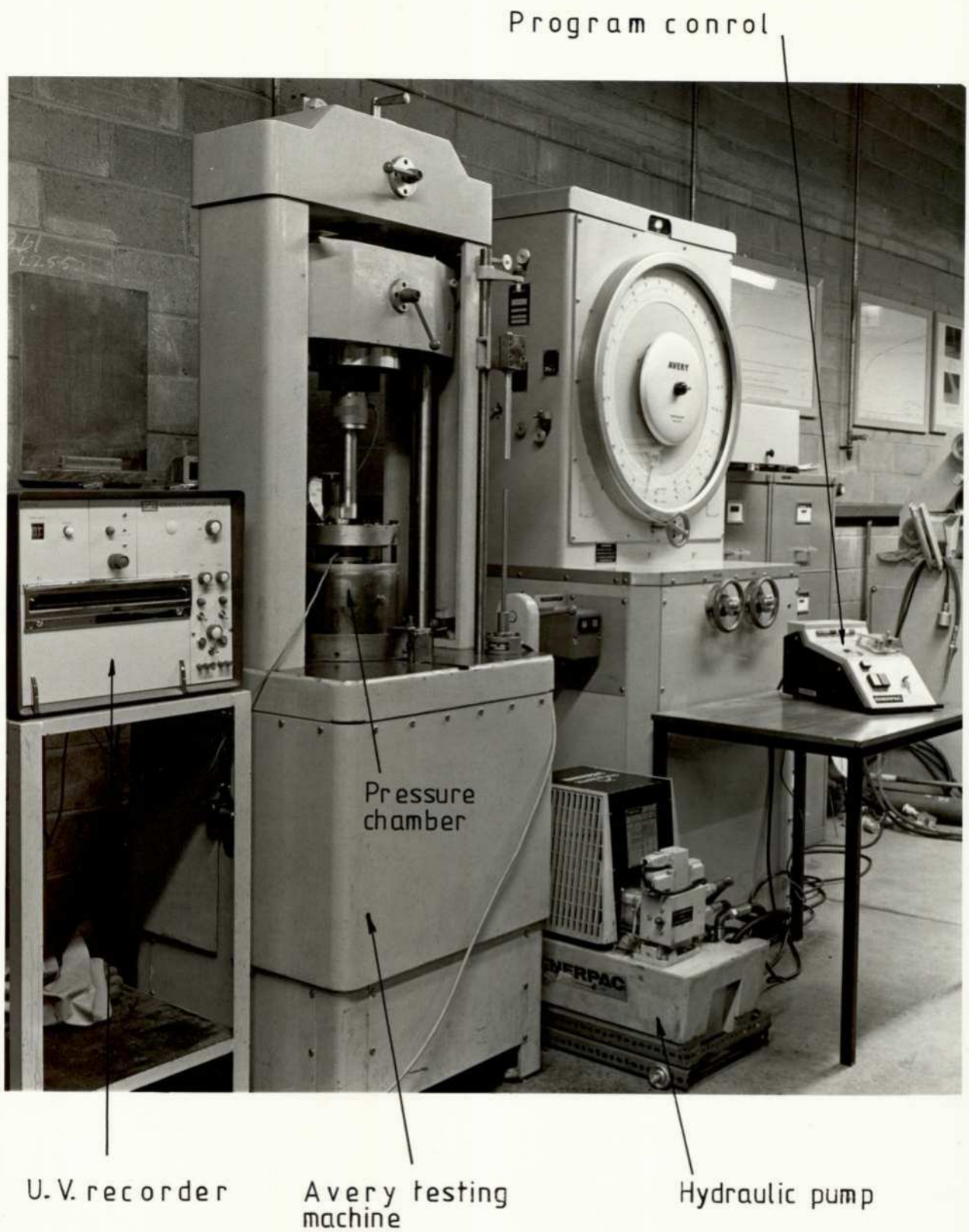


FIGURE 3: GENERAL VIEW OF EQUIPMENT

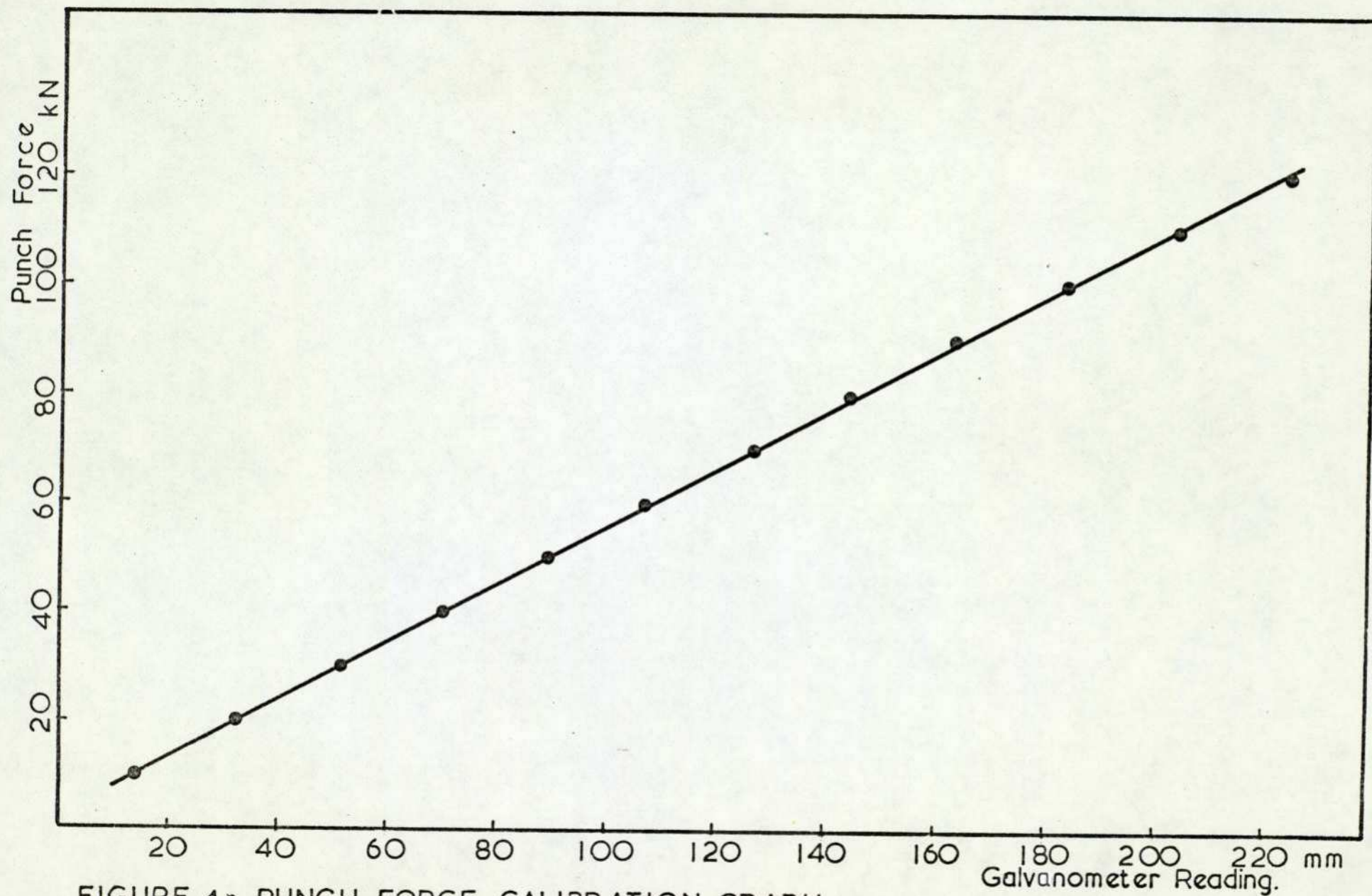


FIGURE 4: PUNCH FORCE CALIBRATION GRAPH

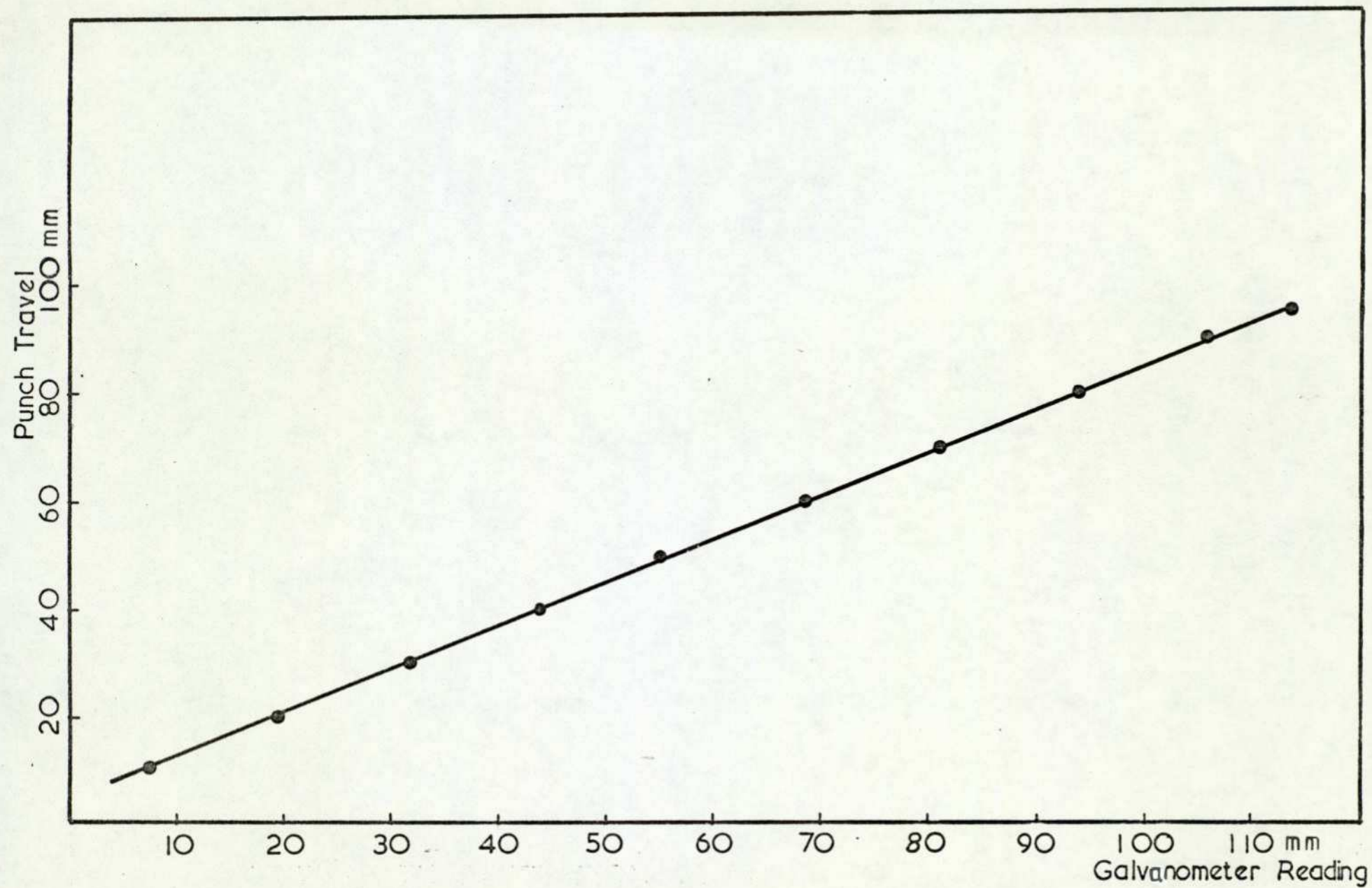


FIGURE 5: PUNCH TRAVEL CALIBRATION GRAPH

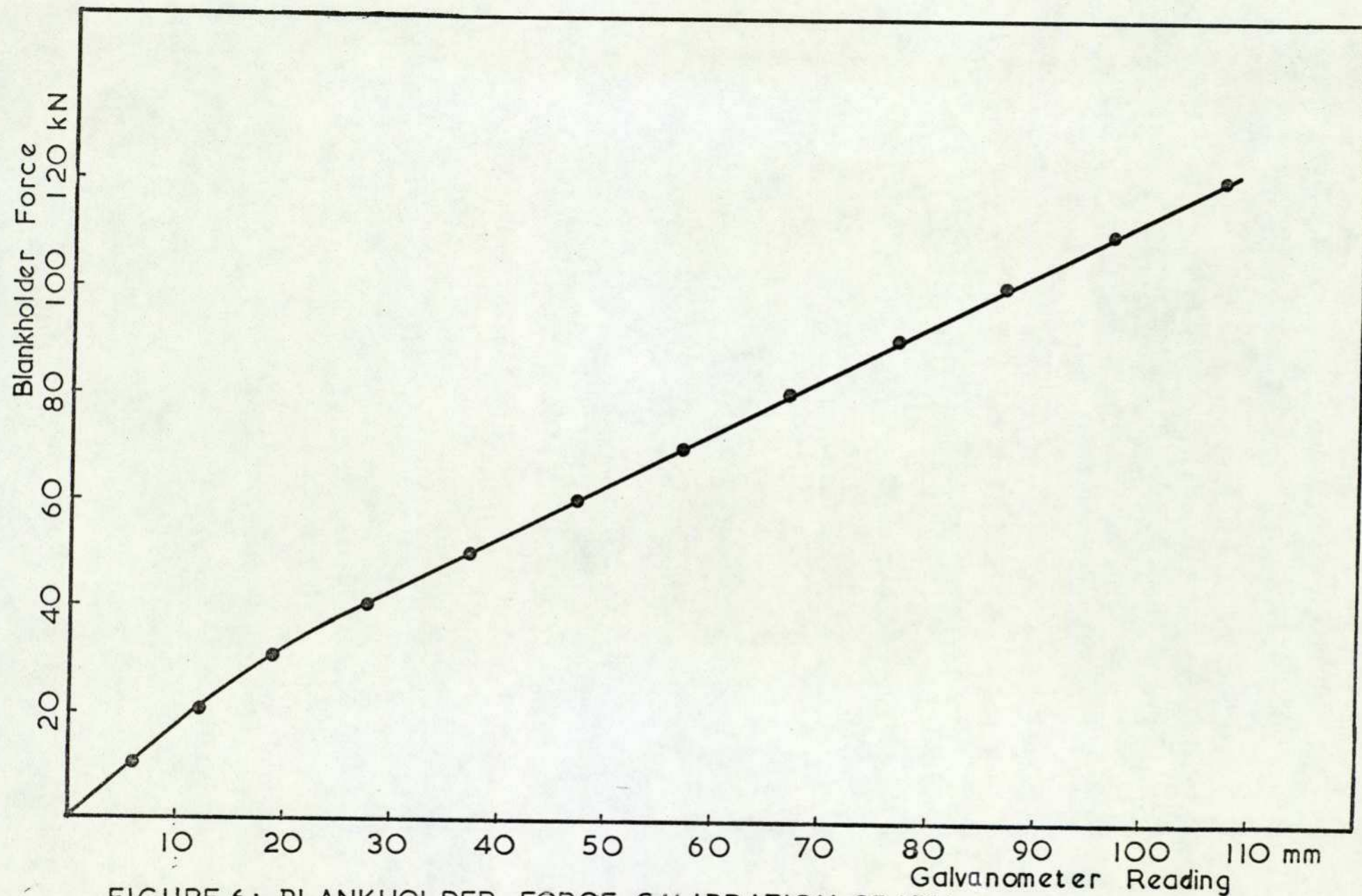


FIGURE 6: BLANKHOLDER FORCE CALIBRATION GRAPH

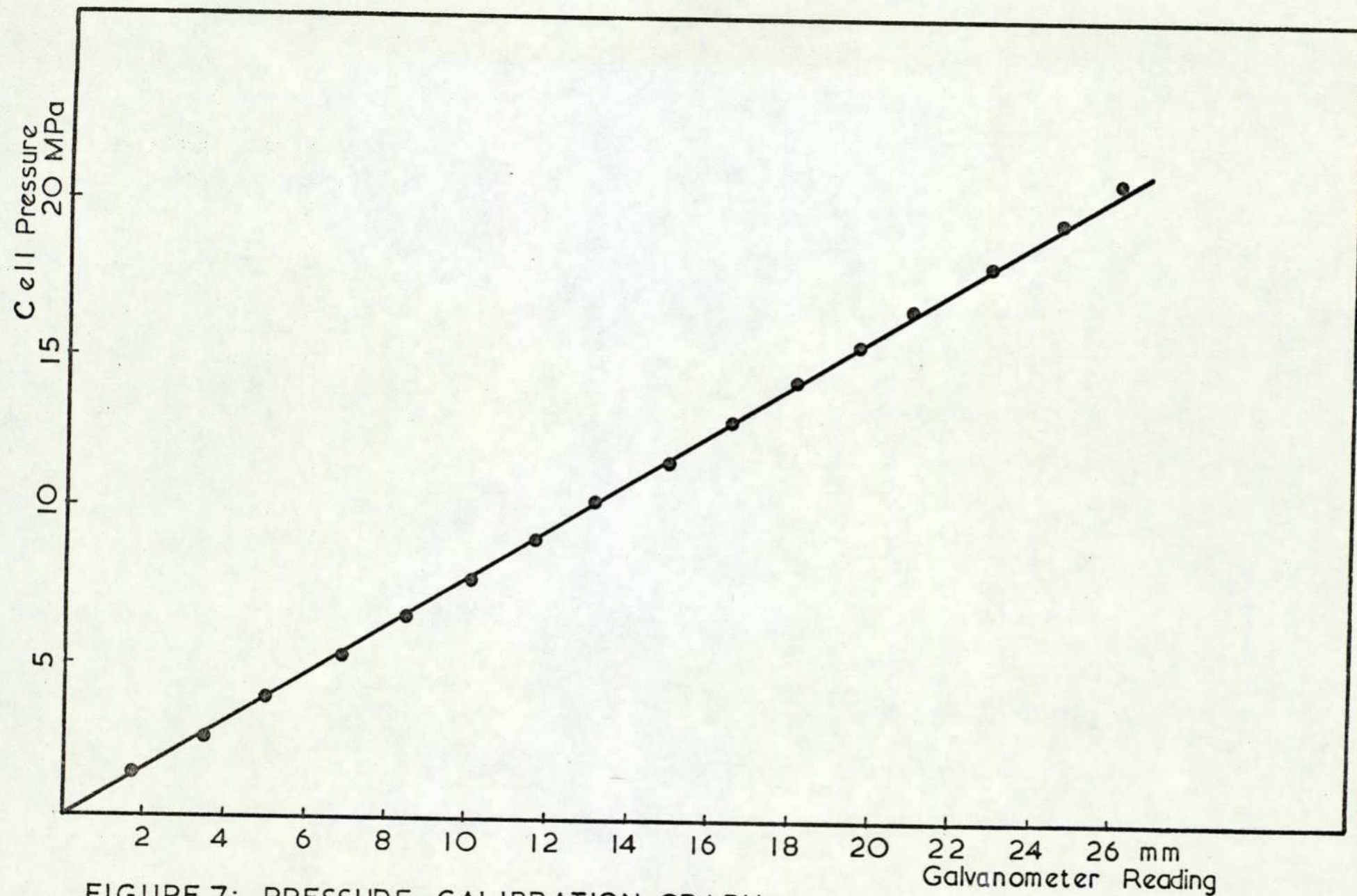


FIGURE 7: PRESSURE CALIBRATION GRAPH

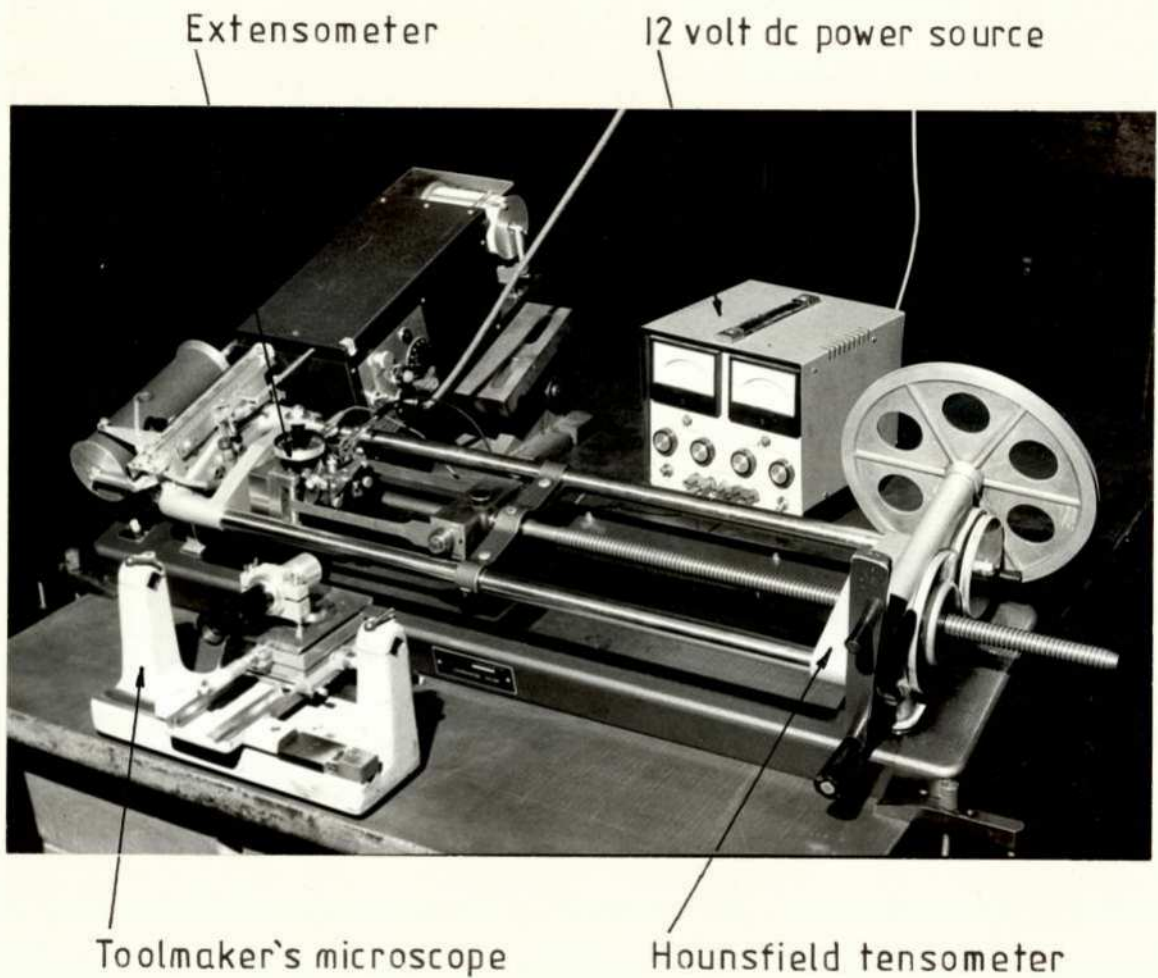


FIGURE 8(a): GENERAL VIEW OF FRICTION APPARATUS

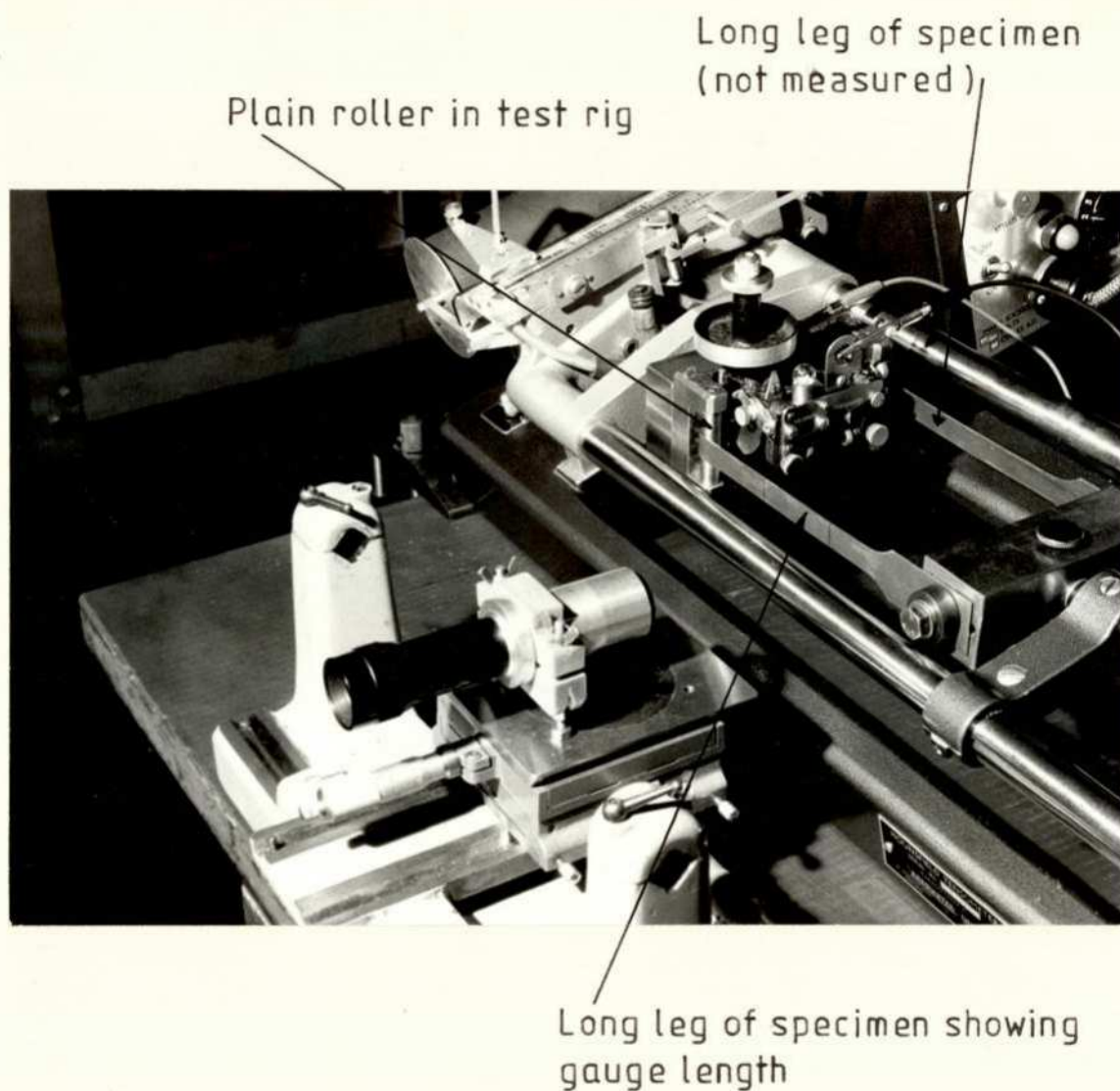


FIGURE 8(b): CLOSE-UP OF FRICTION APPARATUS

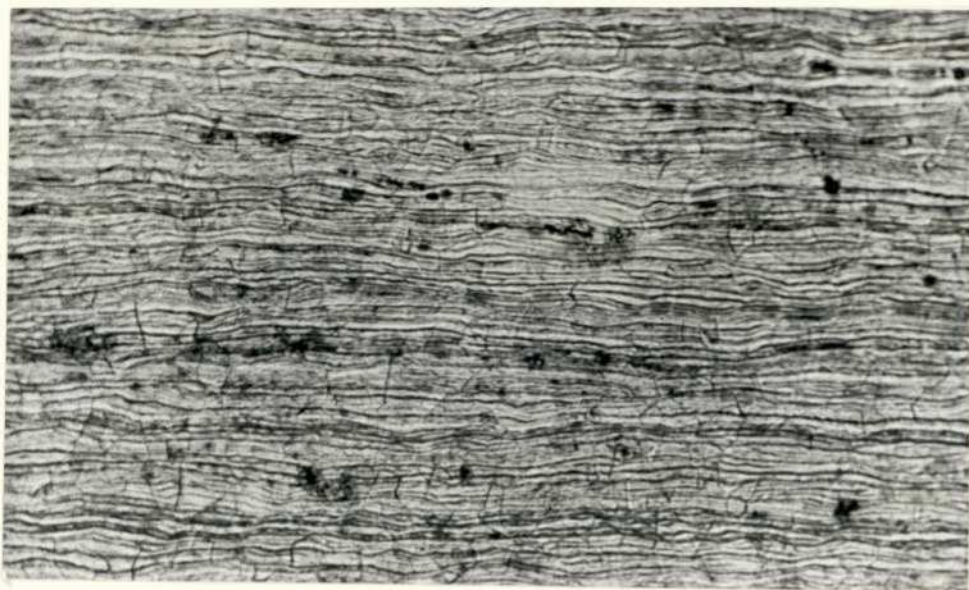


(a) Longitudinal direction x 200



(b) Transverse direction x 200

FIGURE 9; STRUCTURE OF 70/30 BRASS

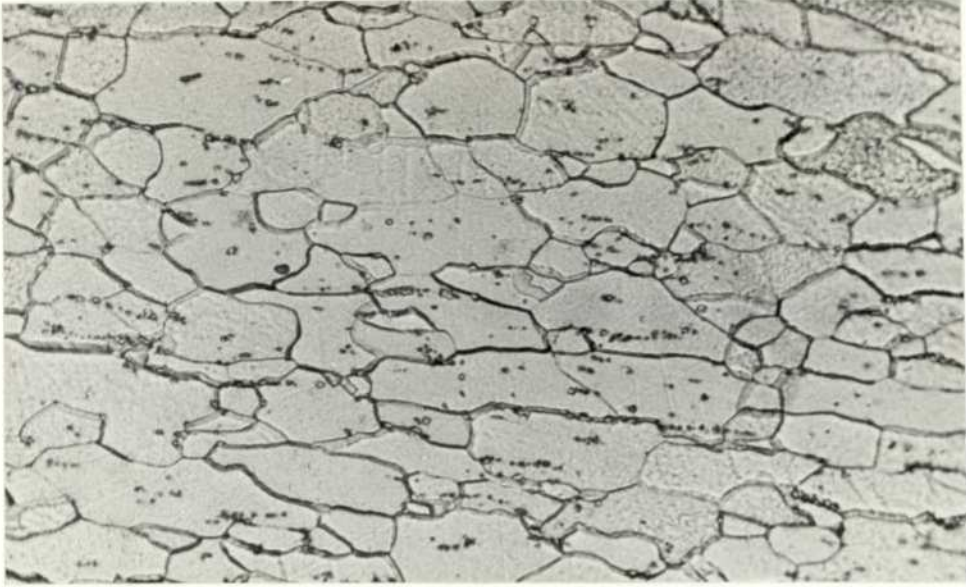


(a) Longitudinal direction x 200



(b) Transverse direction x 200

FIGURE 10: STRUCTURE OF STAINLESS
STEEL En58B



(a) Longitudinal direction x 200

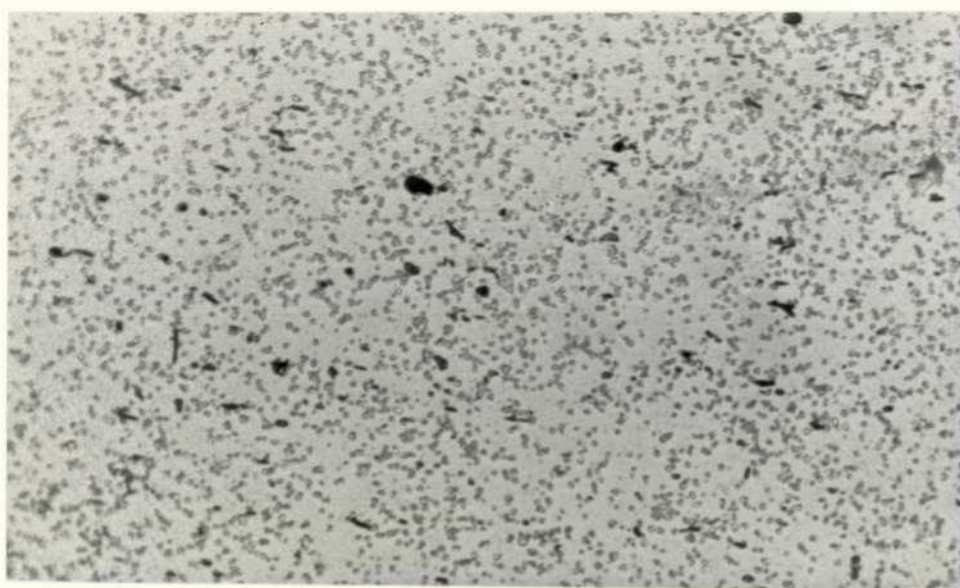


(b) Transverse direction x 200

FIGURE II: STRUCTURE OF MILD STEEL CR4



(a) Longitudinal direction x 500



(b) Transverse direction x 500

FIGURE 12 : STRUCTURE OF ALUMINIUM S1C

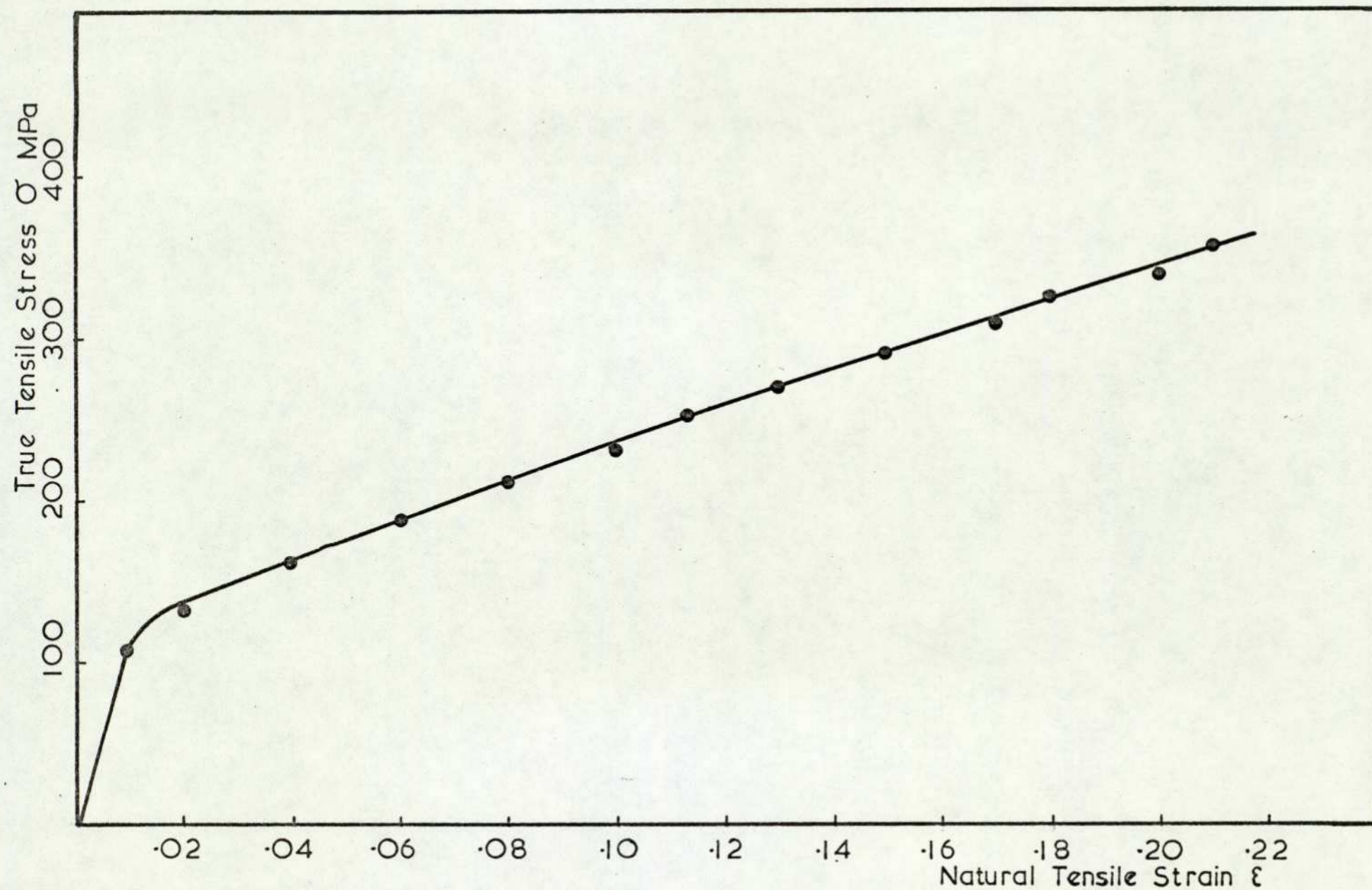


FIGURE 13: TRUE STRESS vs NATURAL STRAIN (70/30 BRASS)

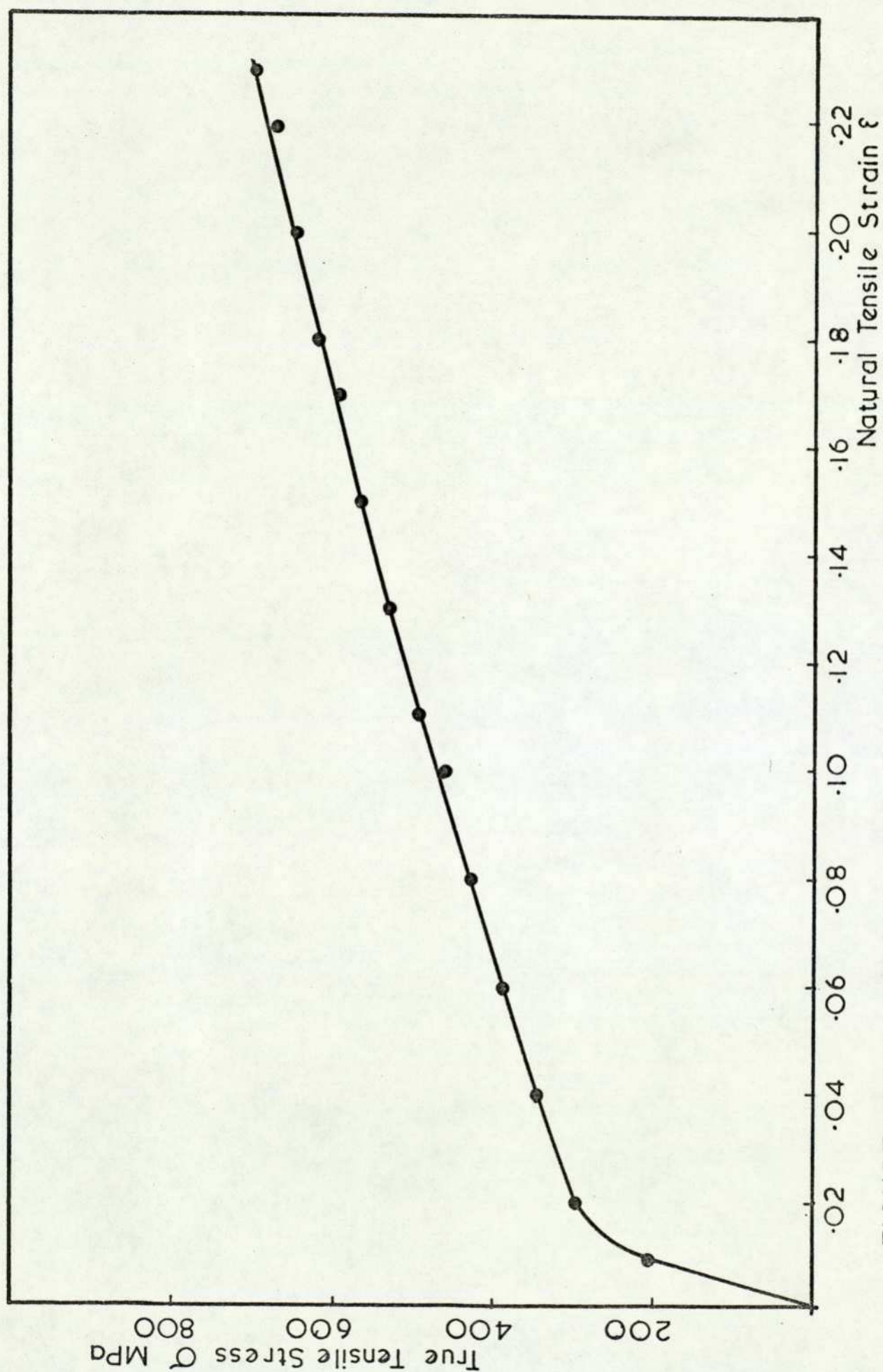


FIGURE 14: TRUE STRESS vs NATURAL STRAIN (STAINLESS STEEL EN 58B)

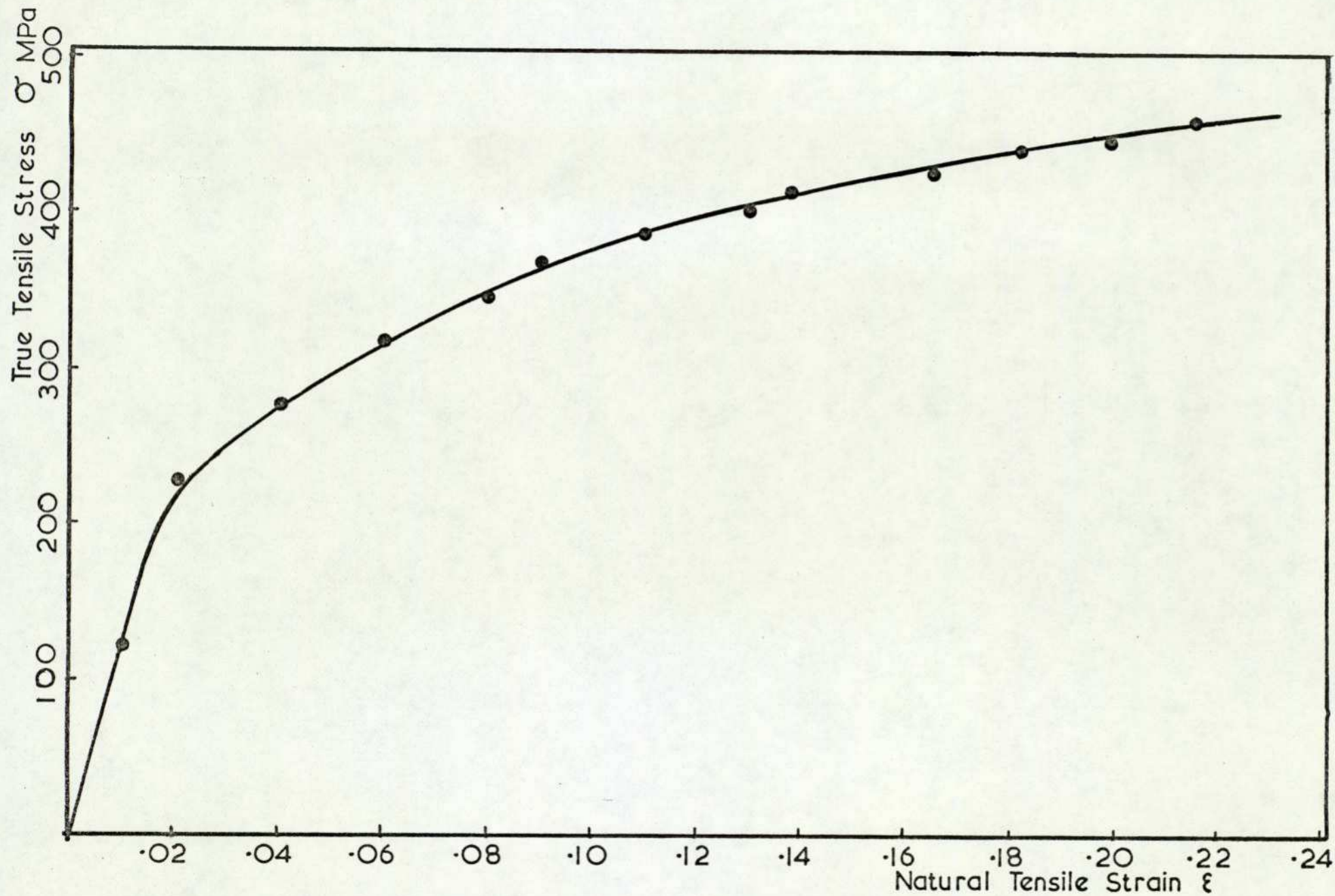


FIGURE 15: TRUE STRESS vs NATURAL STRAIN(MILD STEEL CR4)

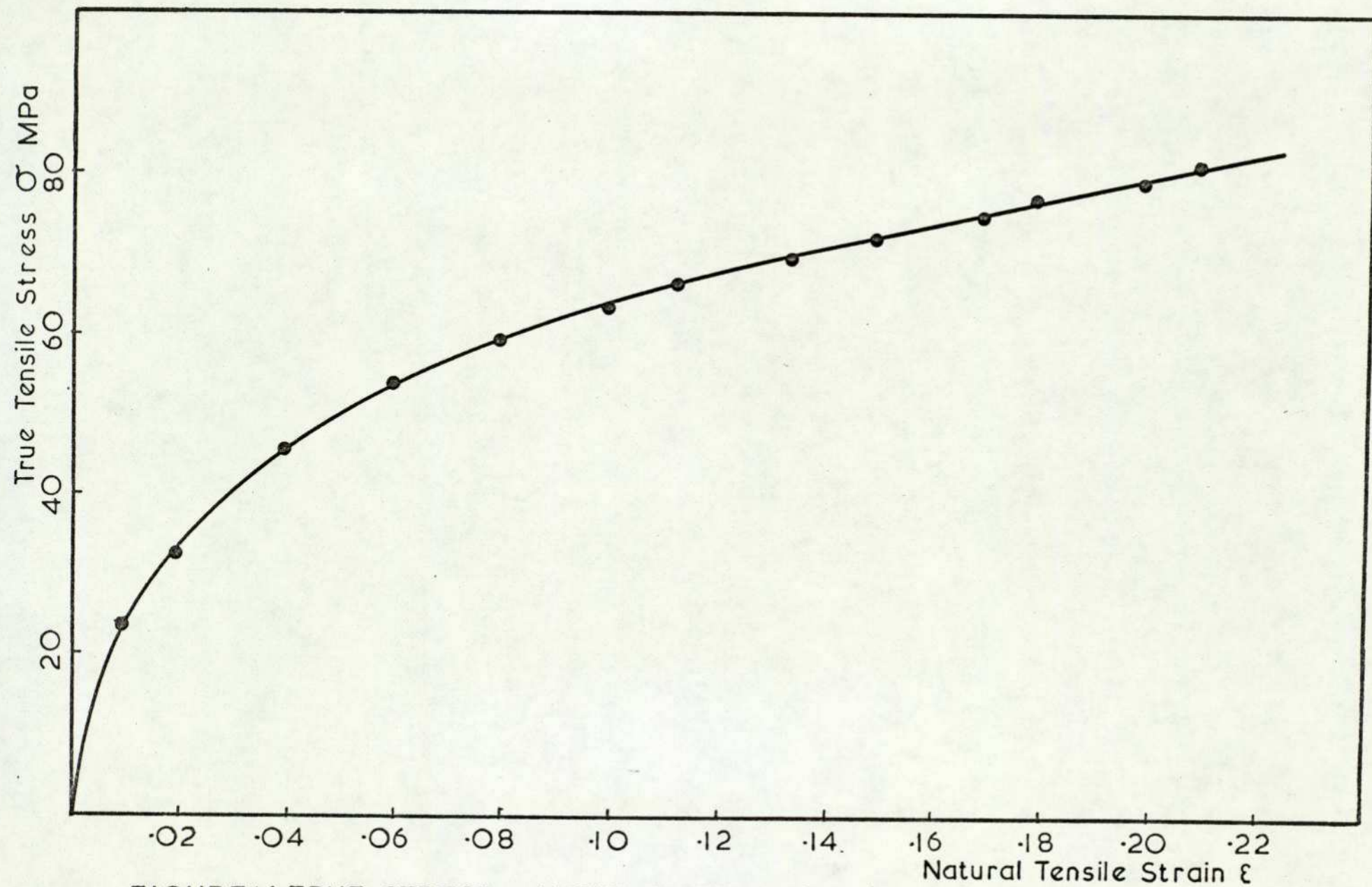


FIGURE 16: TRUE STRESS vs NATURAL STRAIN (ALUMINIUM IC)

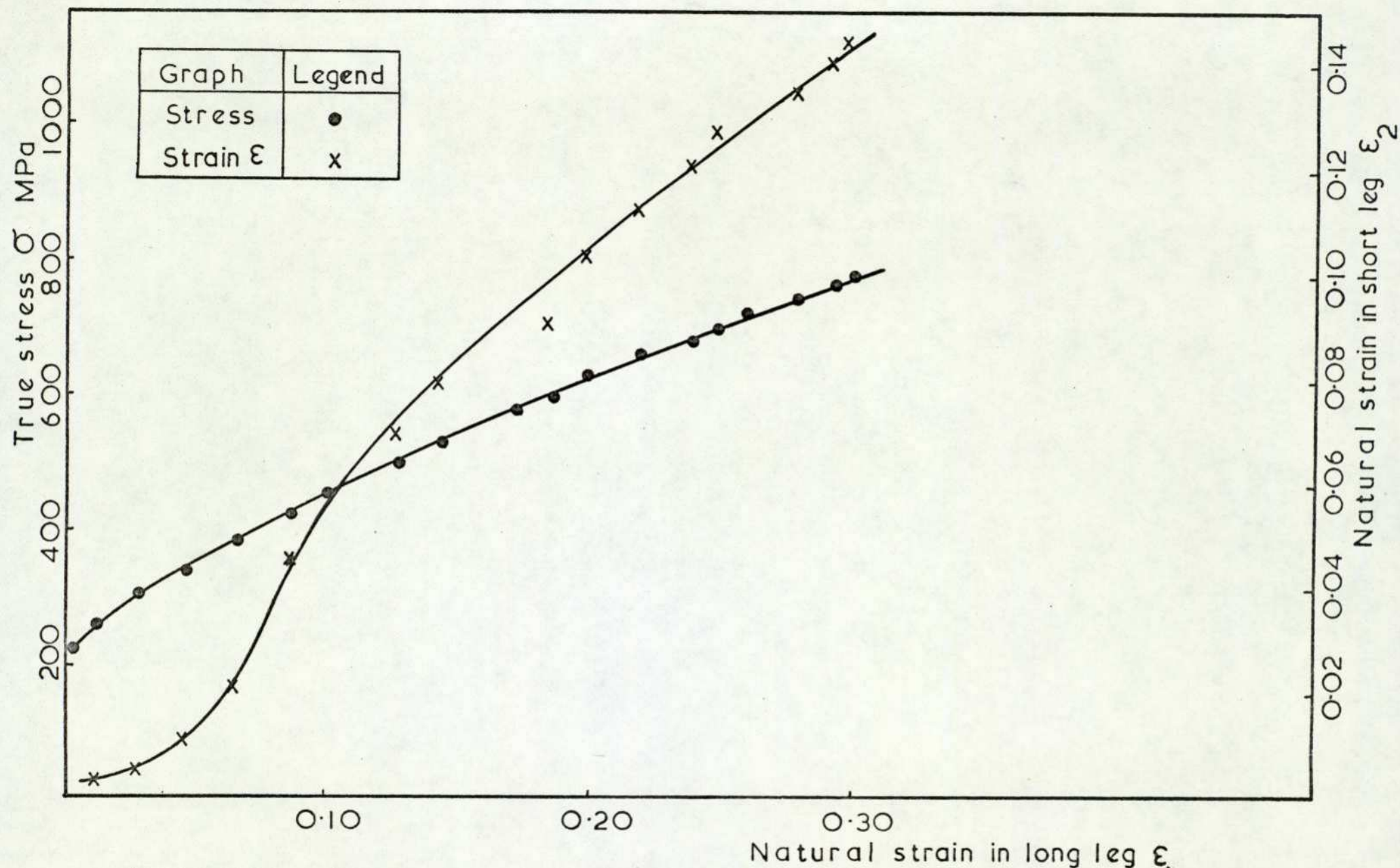
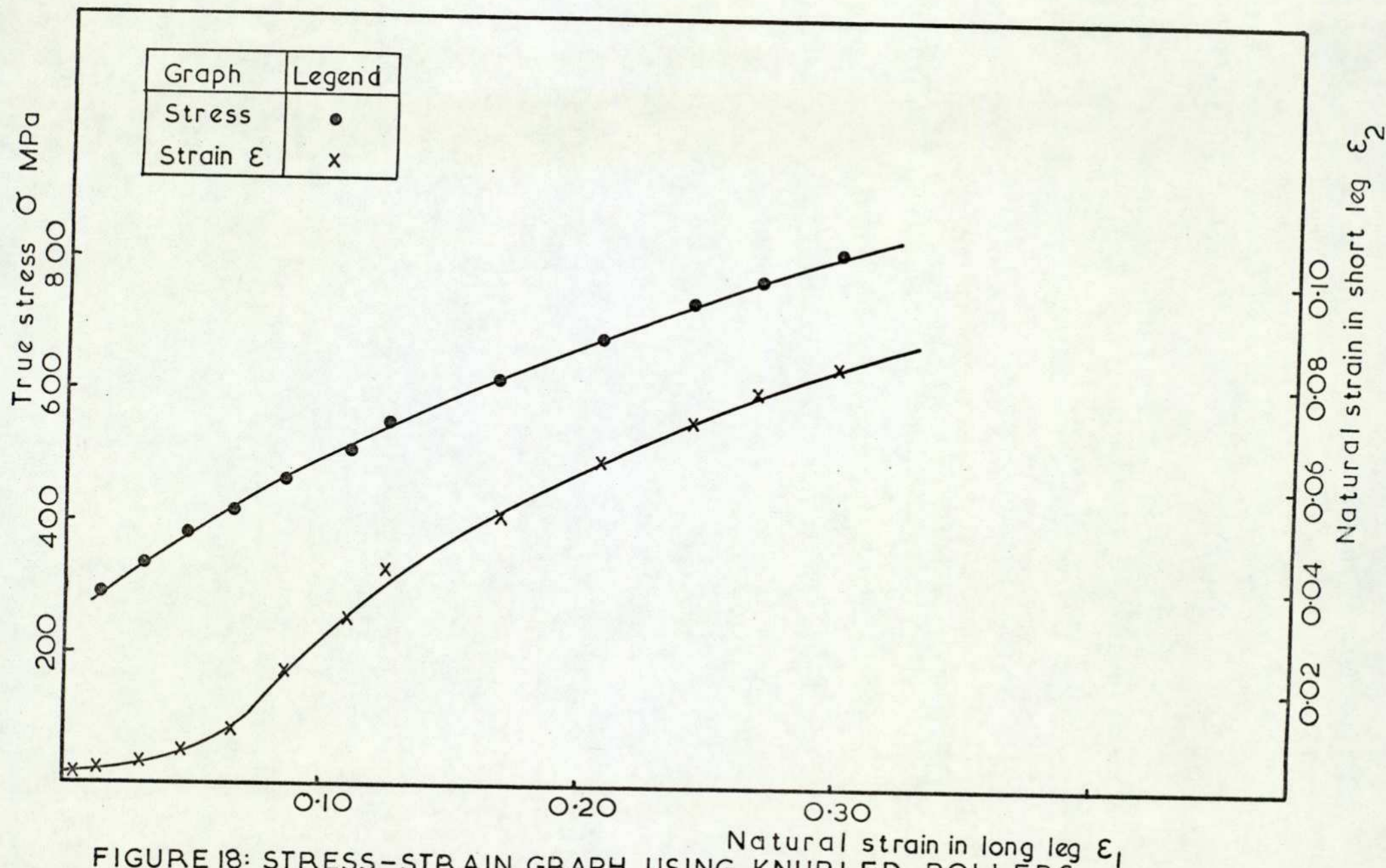


FIGURE 17: STRESS-STRAIN GRAPH USING PLAIN ROLLERS
MATERIAL: STAINLESS STEEL En58B



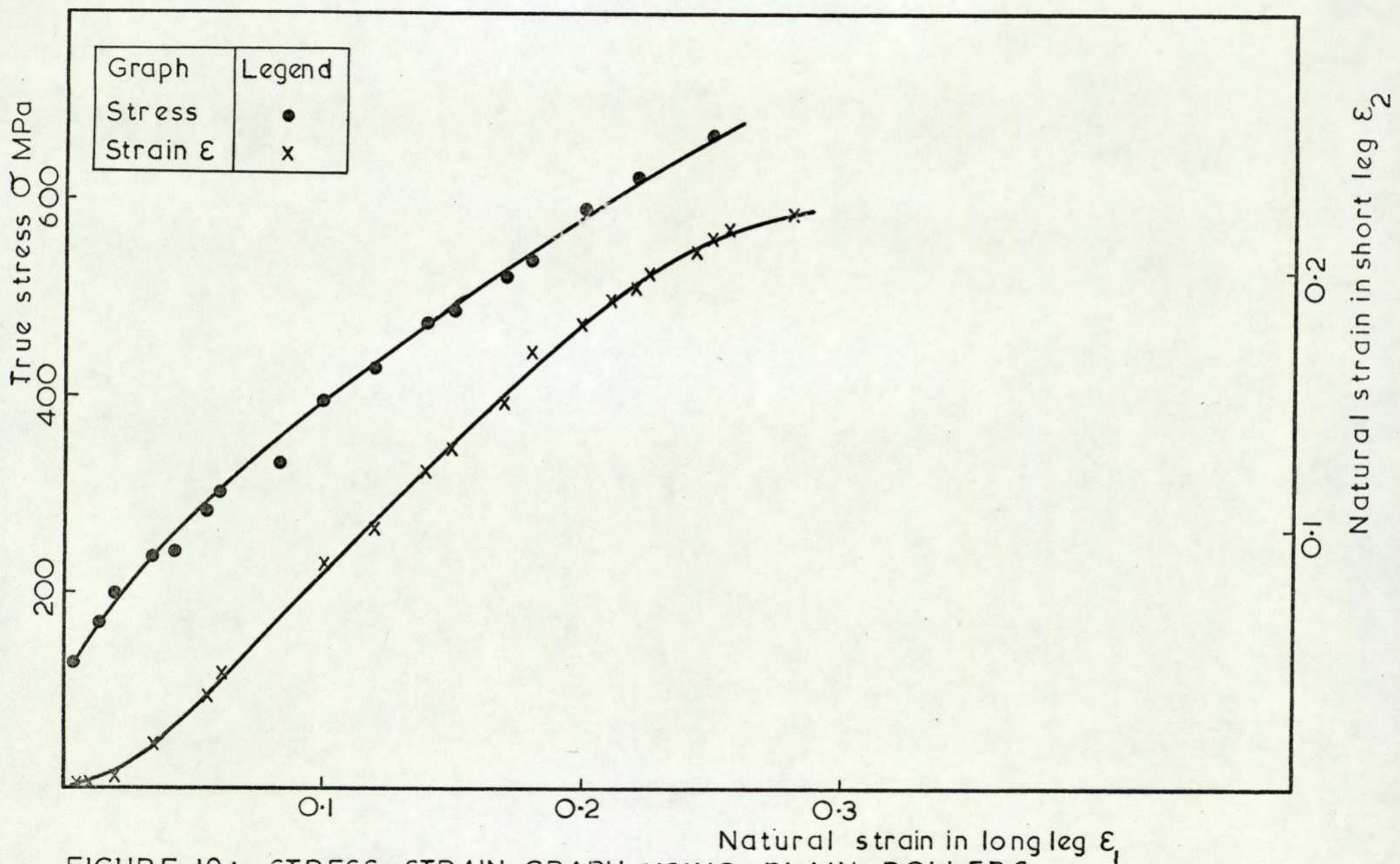


FIGURE 19: STRESS-STRAIN GRAPH USING PLAIN ROLLERS
MATERIAL: 70/30 BRASS

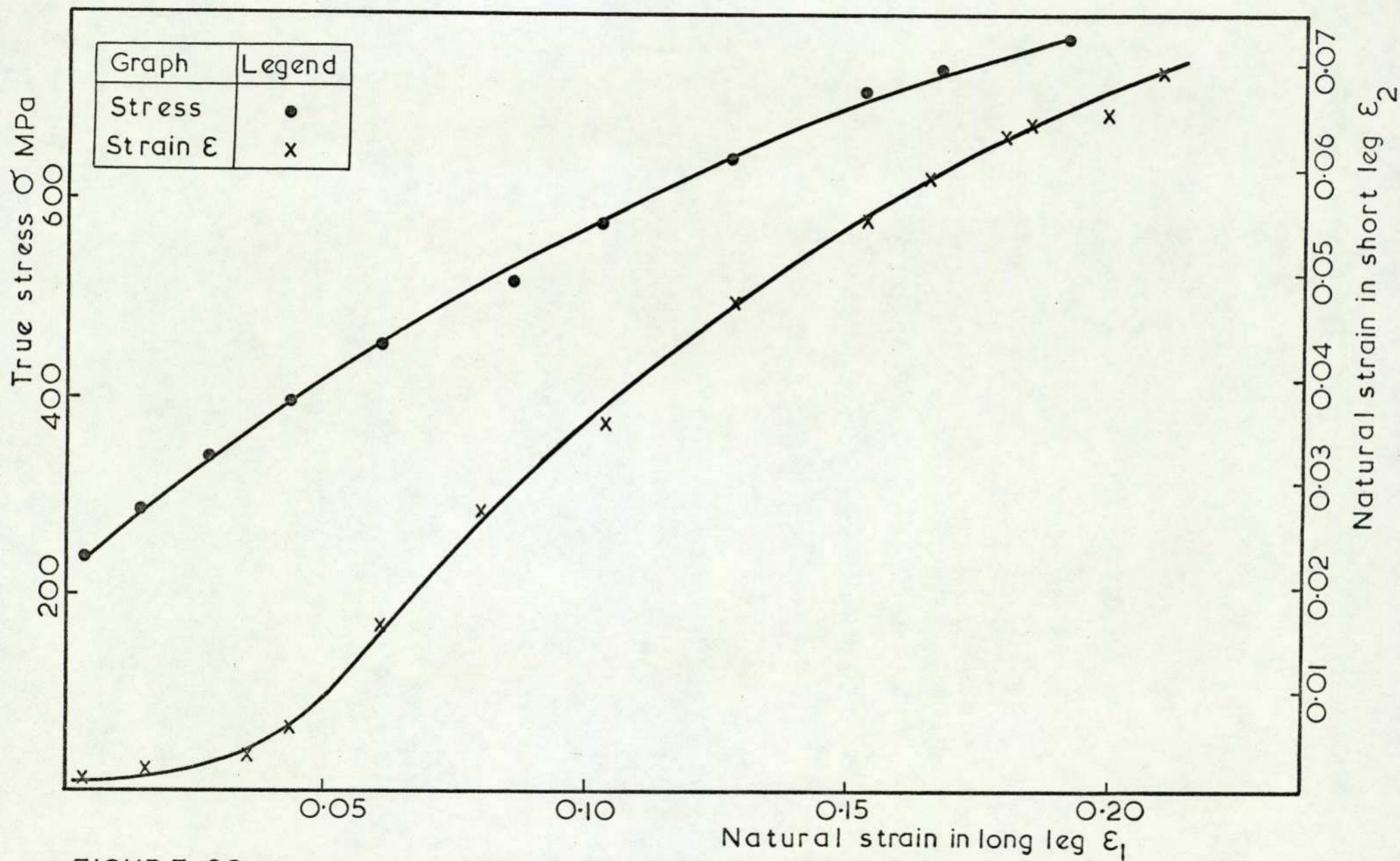


FIGURE 20: STRESS-STRAIN GRAPH USING KNURLED ROLLERS
MATERIAL: 70/30 BRASS

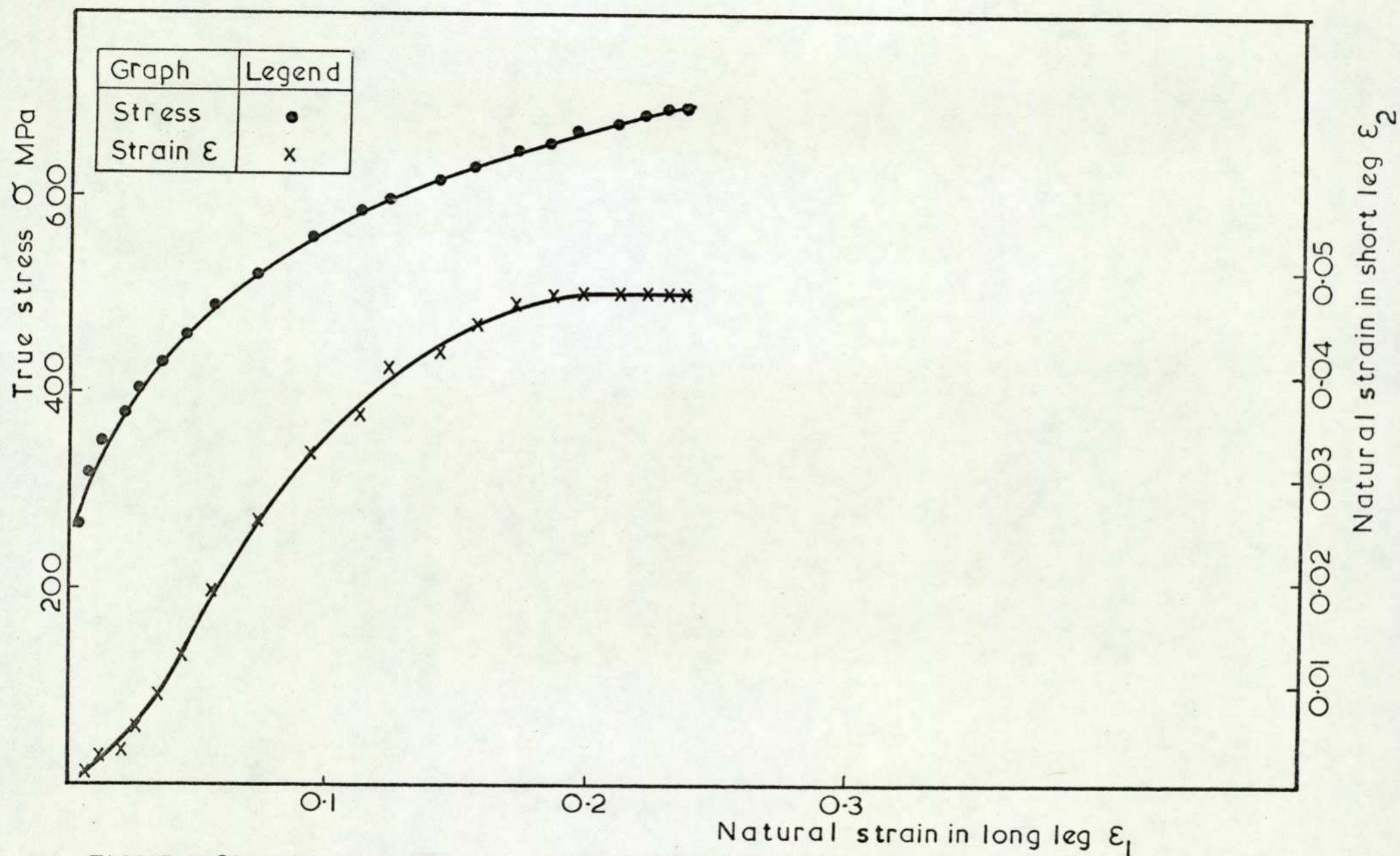


FIGURE 21: STRESS-STRAIN GRAPH USING PLAIN ROLLERS
MATERIAL: MILD STEEL CR4

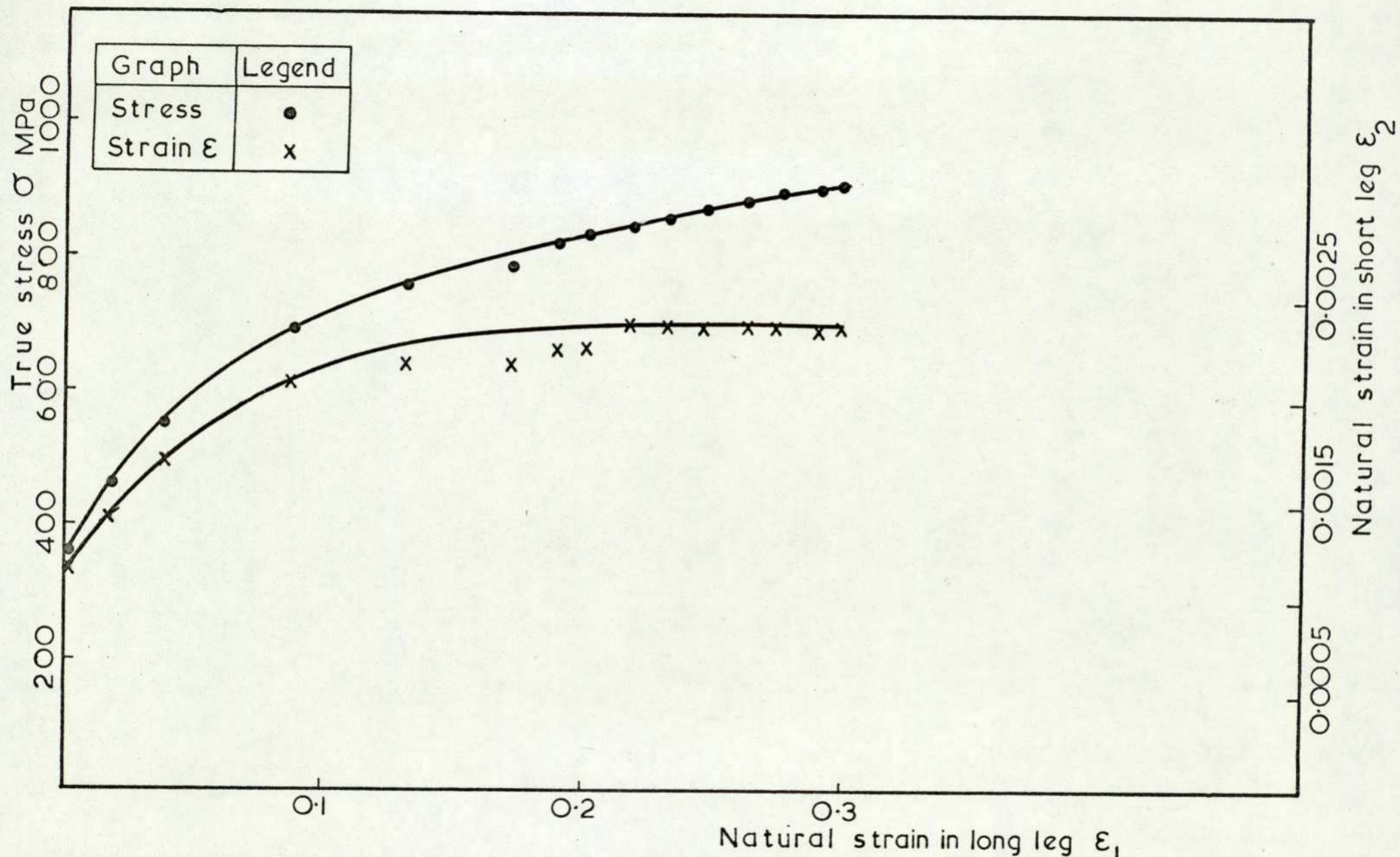


FIGURE 22: STRESS-STRAIN GRAPH 'USING KNURLED' ROLLERS
MATERIAL: MILD STEEL CR4

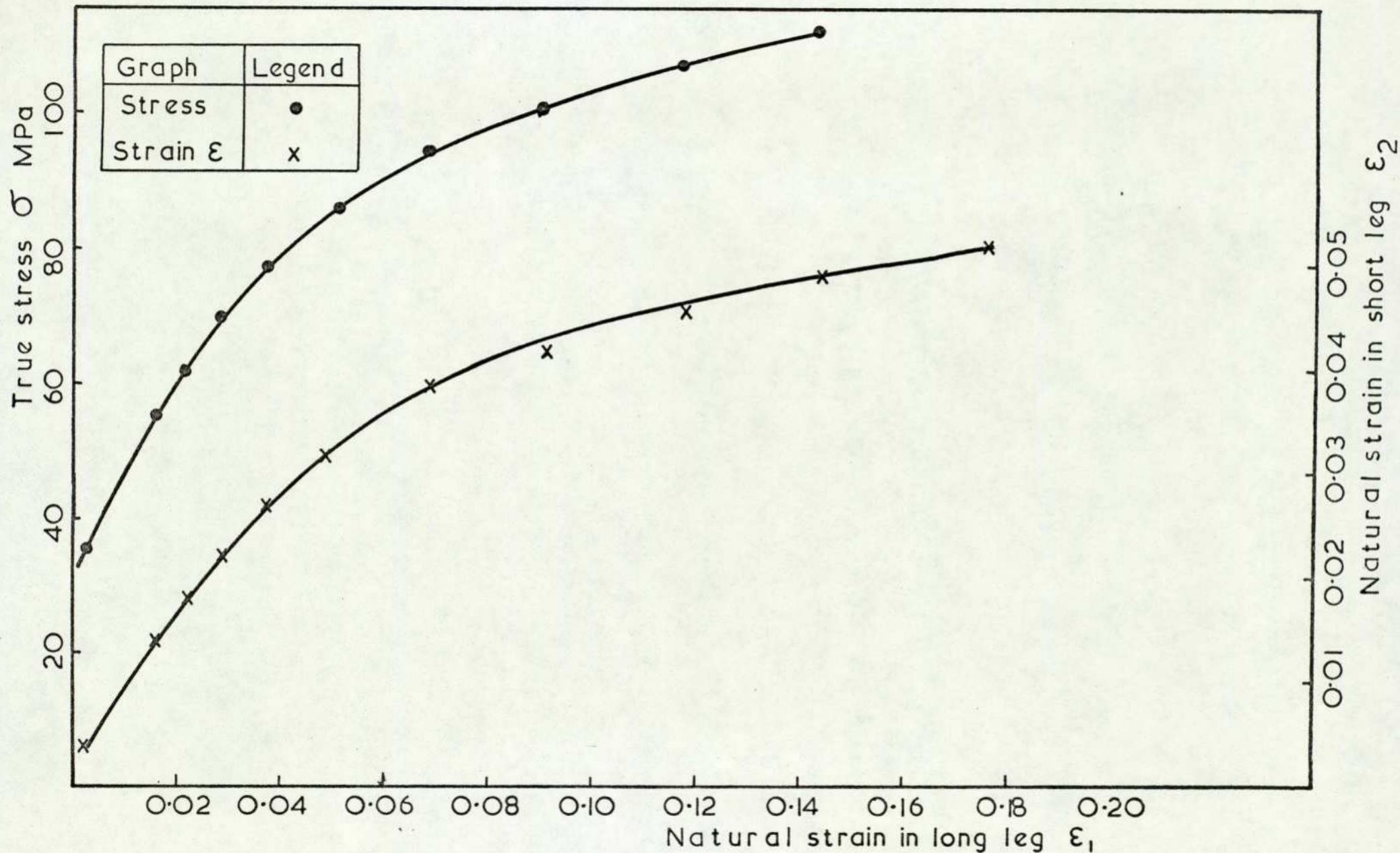


FIGURE 23: STRESS-STRAIN GRAPH USING PLAIN ROLLERS
MATERIAL: ALUMINIUM SiC

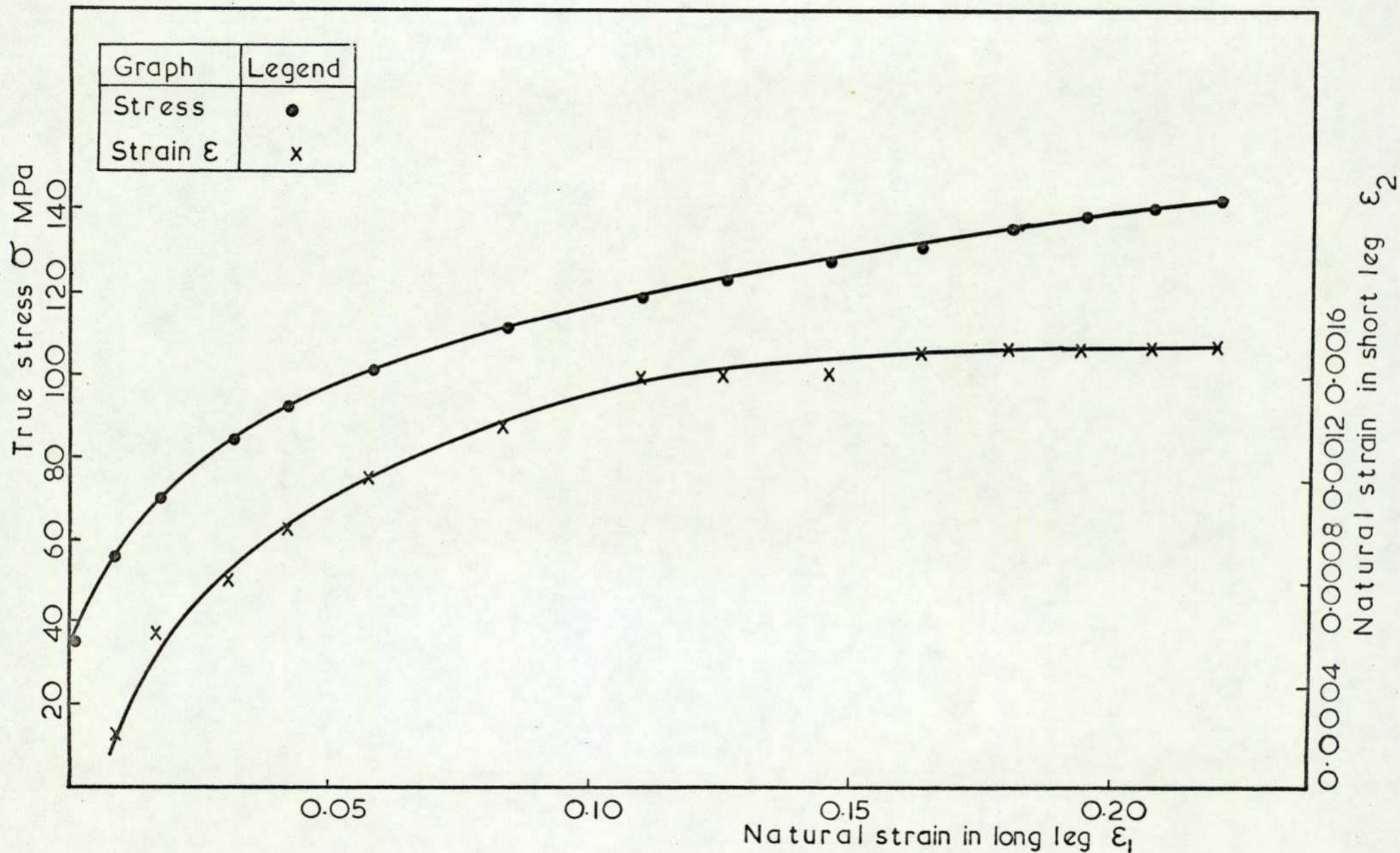


FIGURE 24: STRESS-STRAIN GRAPH USING KNURLED ROLLERS
MATERIAL: ALUMINIUM SiC

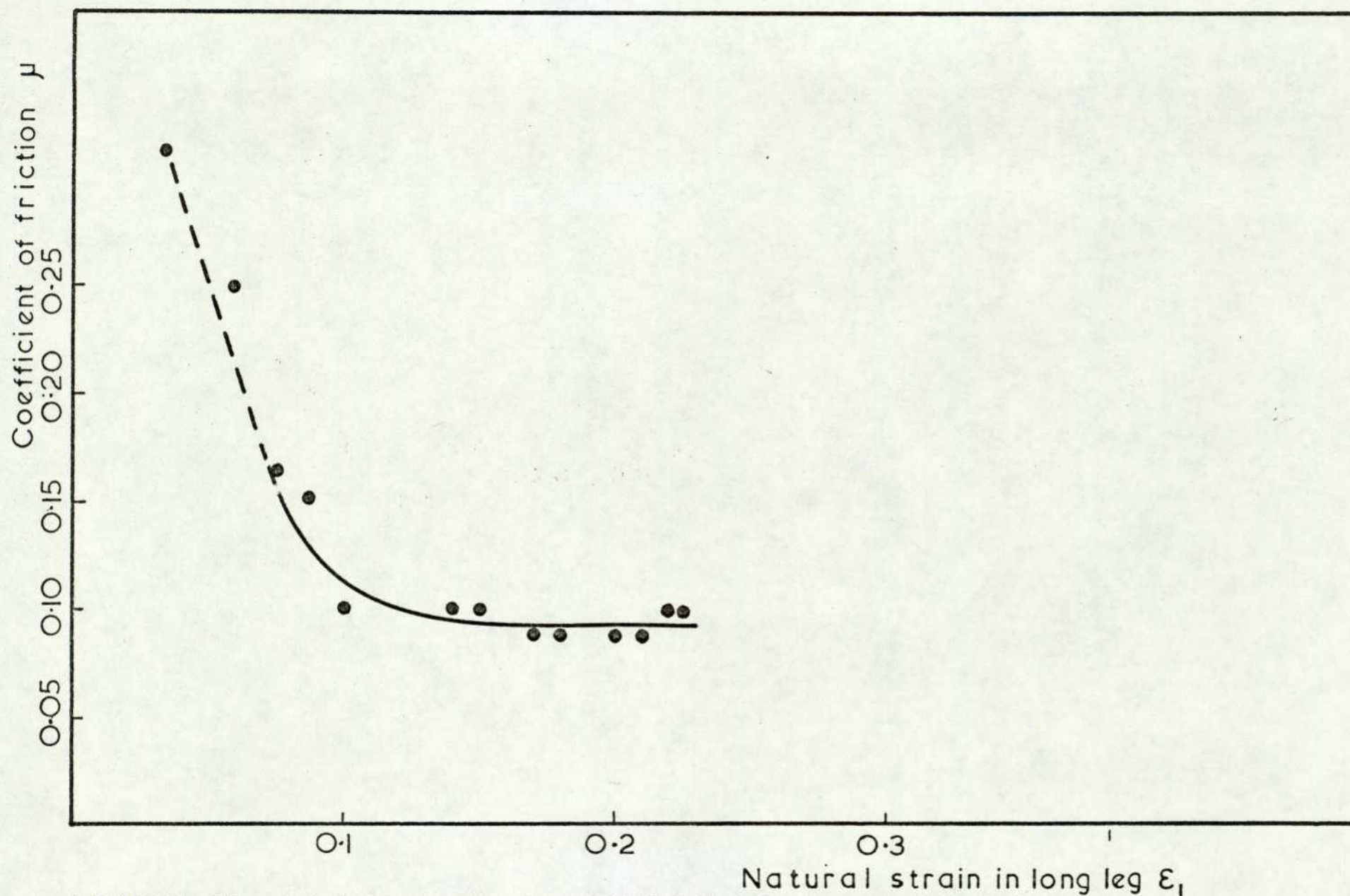


FIGURE 25: COEFFICIENT OF FRICTION USING PLAIN ROLLERS
MATERIAL: 70/30 BRASS

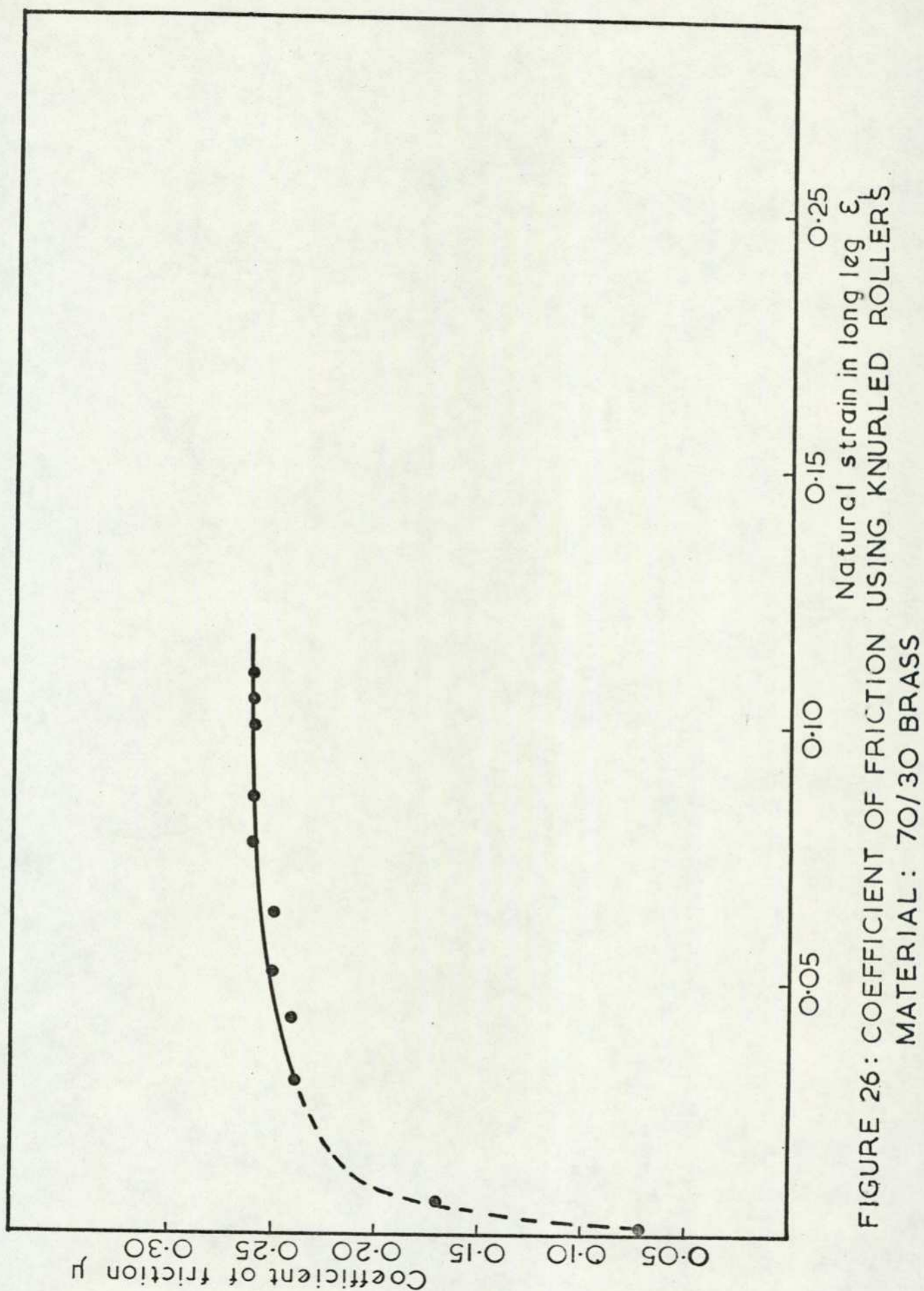


FIGURE 26: COEFFICIENT OF FRICTION
MATERIAL: 70/30 BRASS

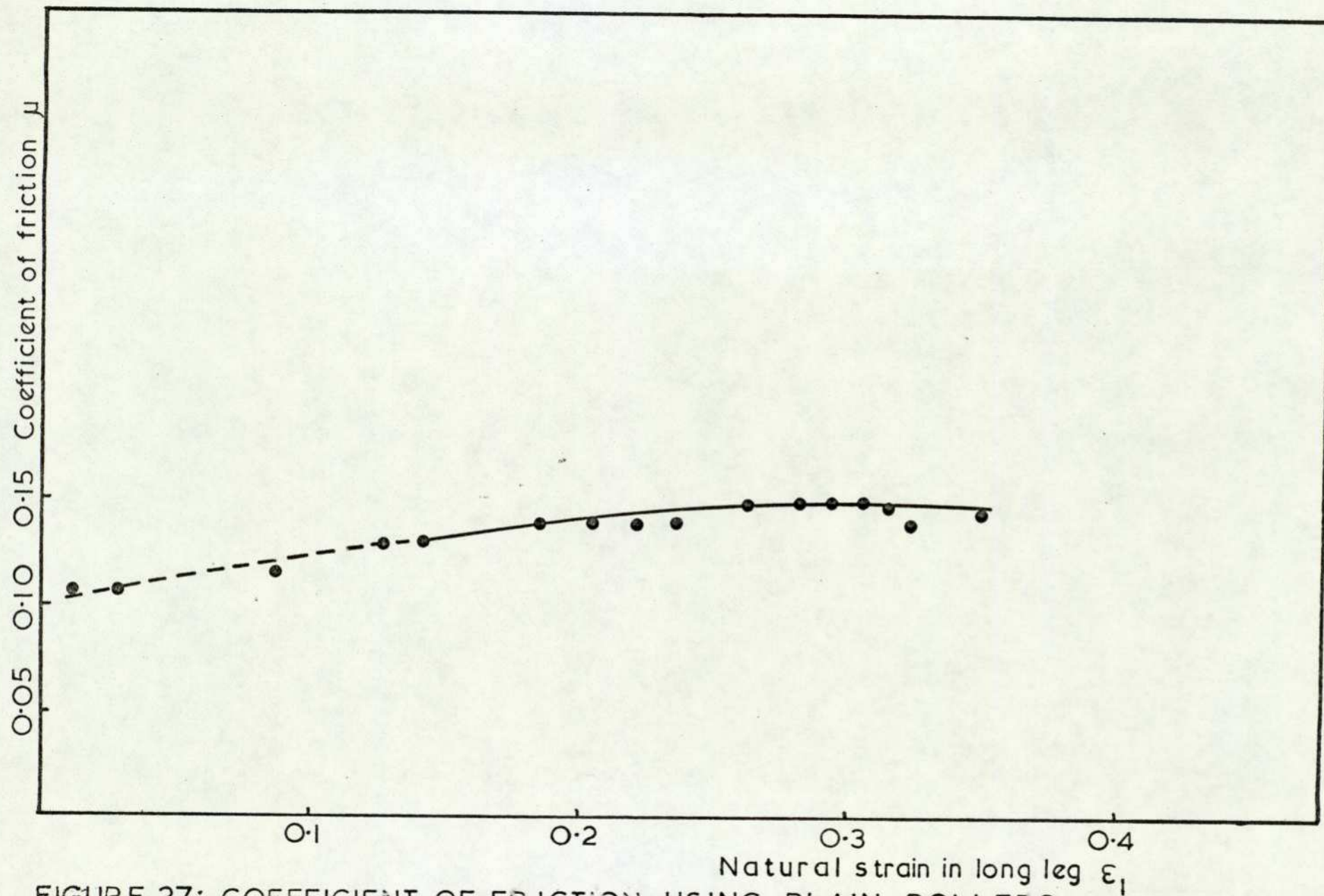


FIGURE 27: COEFFICIENT OF FRICTION USING PLAIN ROLLERS
MATERIAL: STAINLESS STEEL En58B

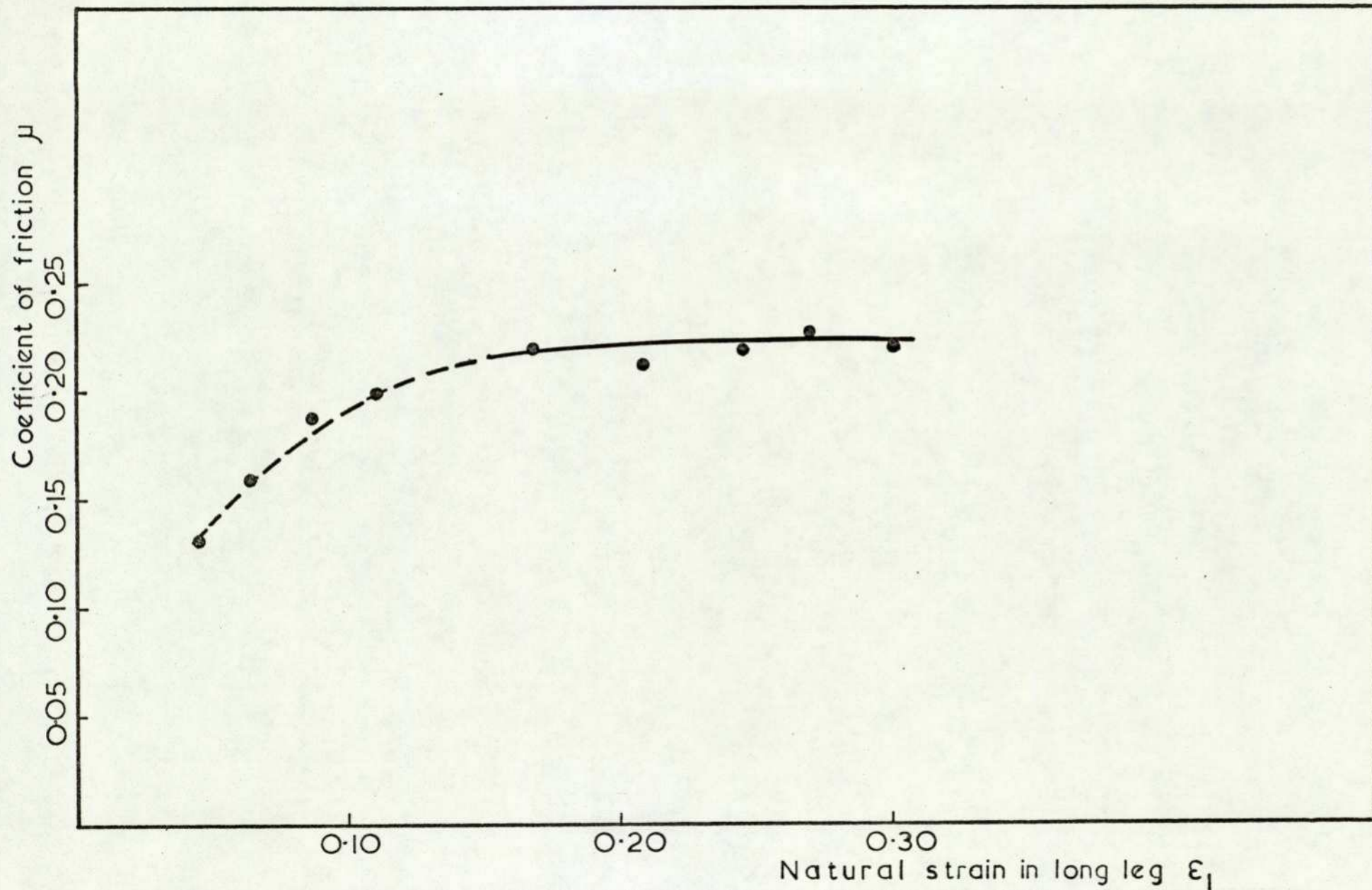


FIGURE 28: COEFFICIENT OF FRICTION USING KNURLED ROLLERS

MATERIAL: STAINLESS STEEL En58B

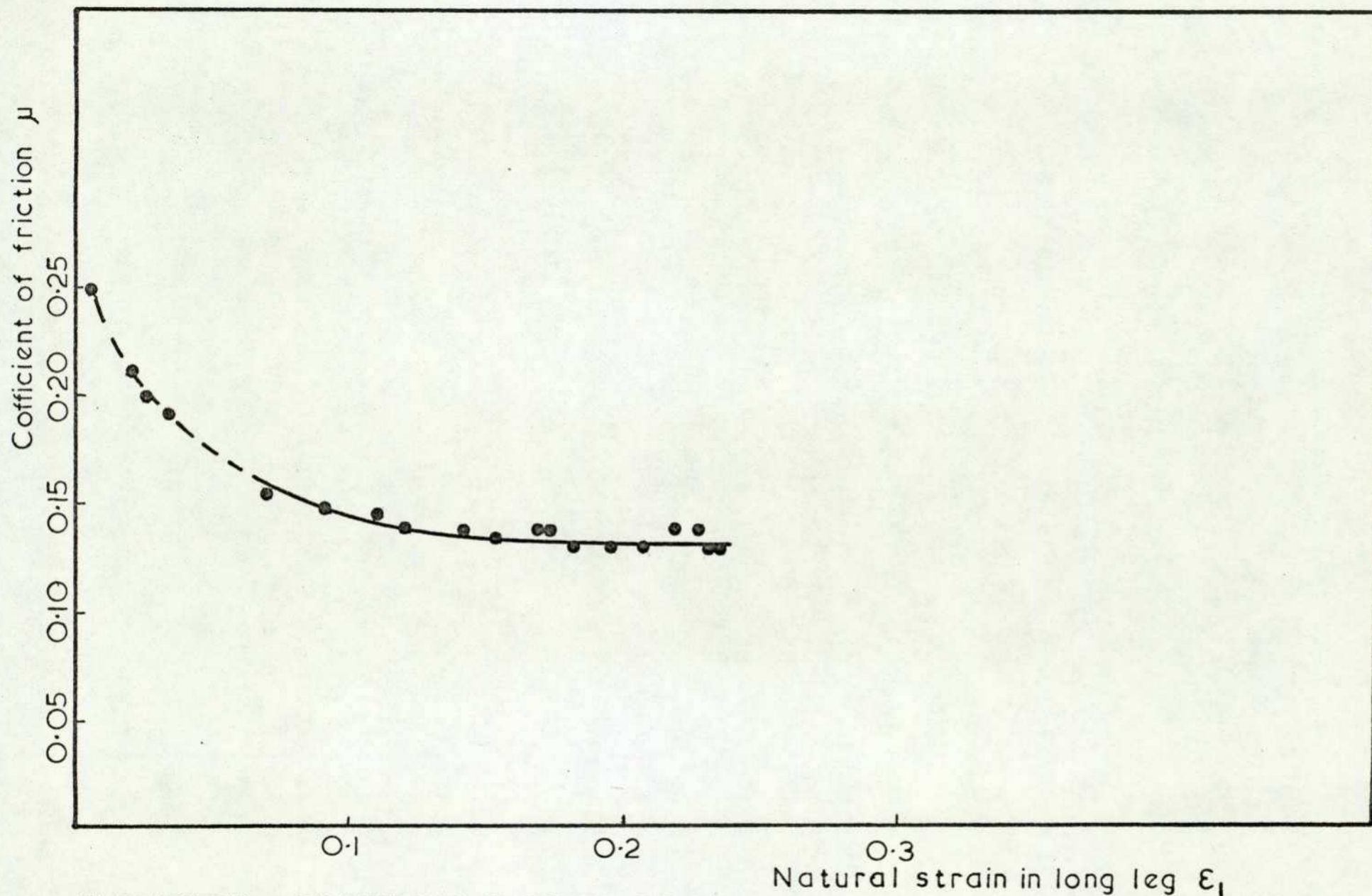


FIGURE 29: COEFFICIENT OF FRICTION USING PLAIN ROLLERS
MATERIAL: MILD STEEL CR4

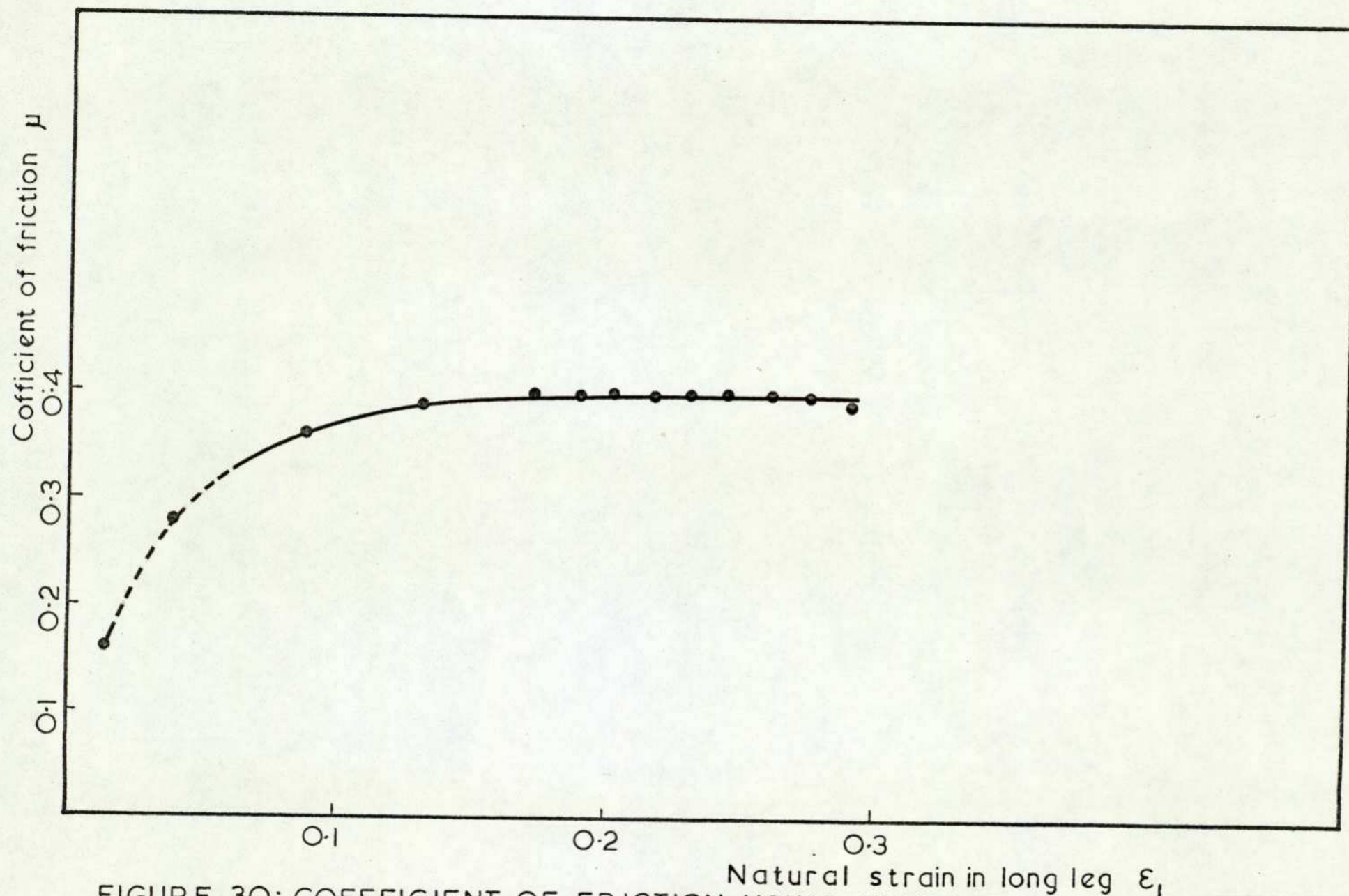


FIGURE 30: COEFFICIENT OF FRICTION USING KNURLED ROLLERS
MATERIAL: MILD STEEL CR4

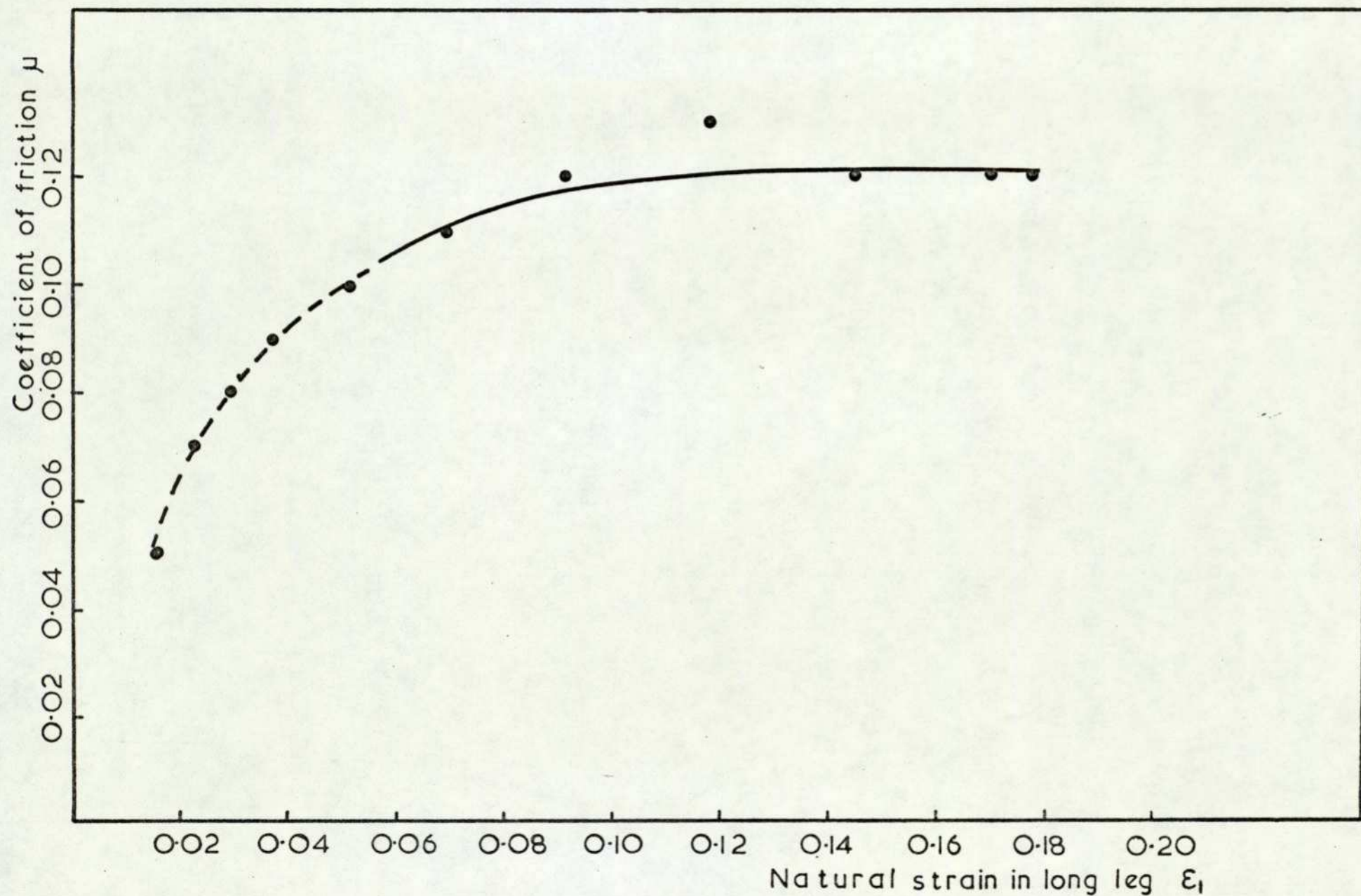


FIGURE 31: COEFFICIENT OF FRICTION USING PLAIN ROLLERS

MATERIAL: ALUMINIUM SIC

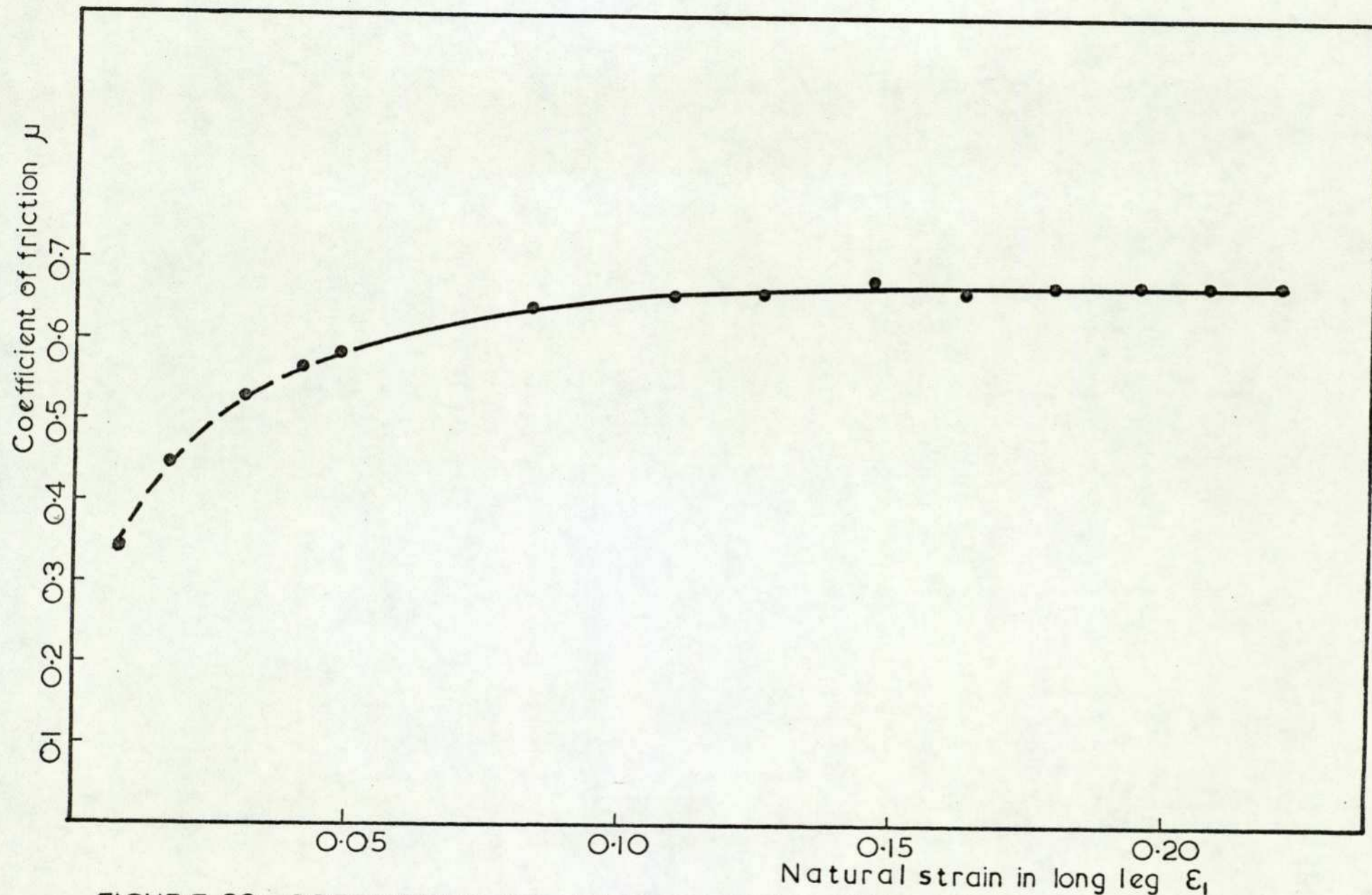


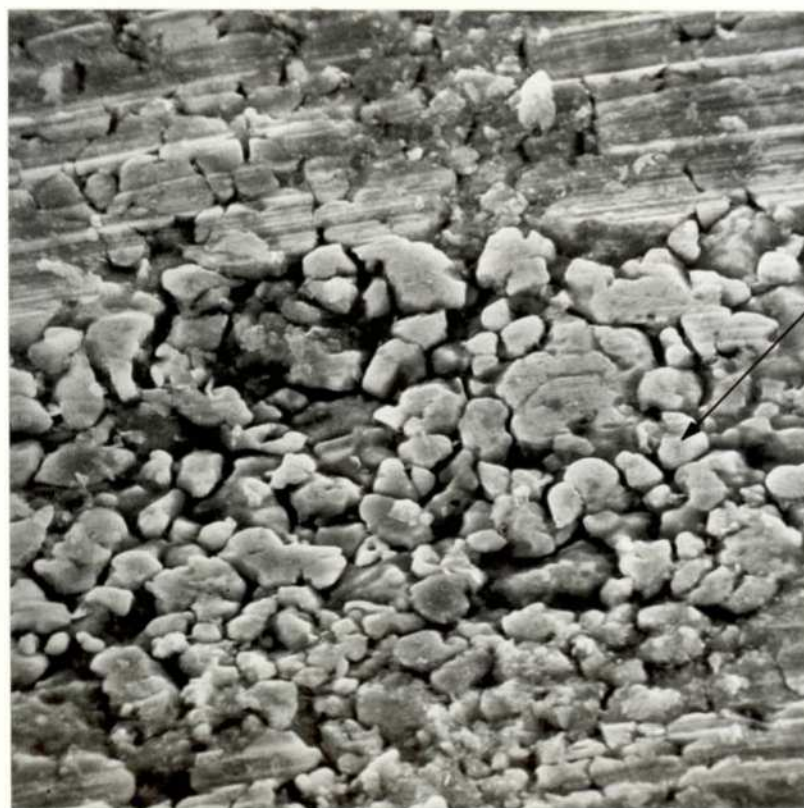
FIGURE 32: COEFFICIENT OF FRICTION USING KNURLED ROLLERS
MATERIAL: ALUMINIUM SIC

Surface levelled by rollers



(a) x 500

Original surface



(b) x 2000

FIGURE 33: SCANNING ELECTRON MICROGRAPH
STAINLESS STEEL En58B - PLAIN ROLLERS



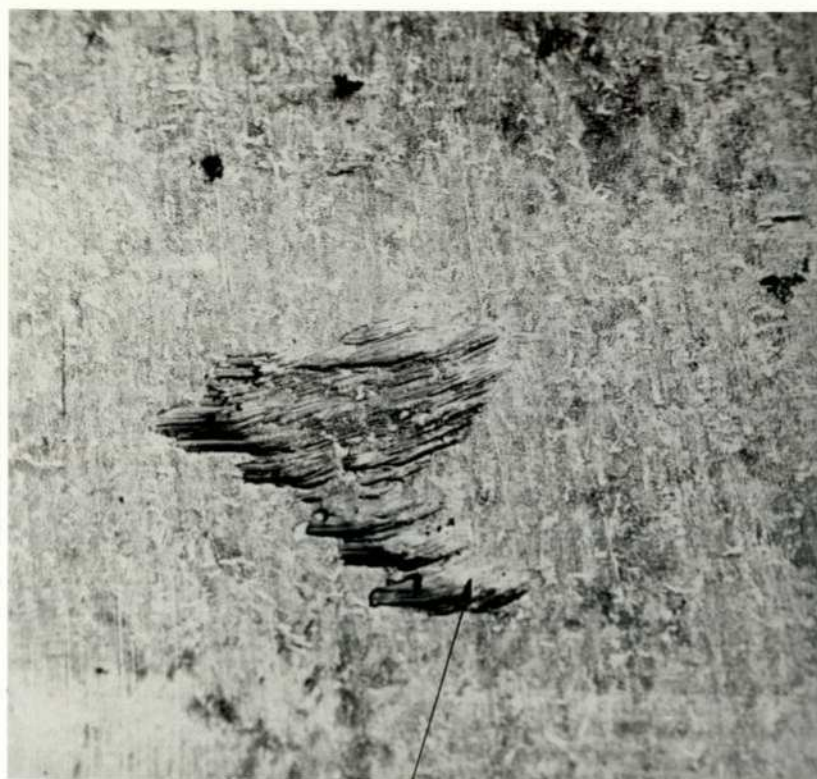
(a) x 500

Shallow grooving



(b) x 1000

FIGURE 34: SCANNING ELECTRON MICROGRAPH
STAINLESS STEEL EN58B—KNURLED ROLLERS



(a) x 100

Local welding and fracture



(b) x 500

FIGURE 35: SCANNING ELECTRON MICROGRAPH
70/30 BRASS — PLAIN ROLLERS

Ploughing of surface of material



(a) x 50

Debris in the form of shavings



(b) x 5000

FIGURE 36: SCANNING ELECTRON MICROGRAPH
70/30 BRASS — KNURLED ROLLERS



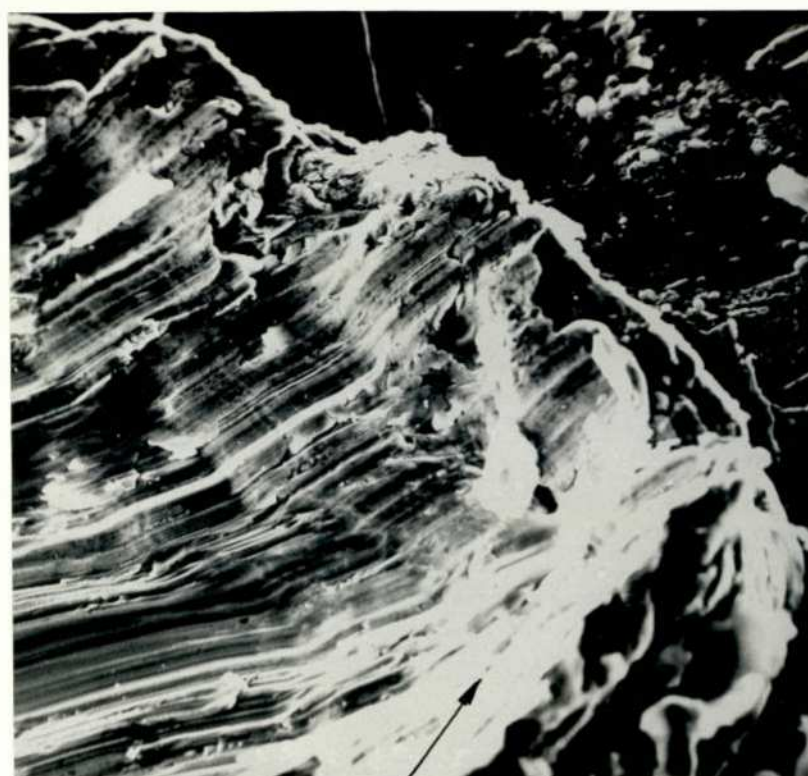
(a) x 500

Levelled surface



(b) x 2000

FIGURE 37: SCANNING ELECTRON MICROGRAPH
MILD STEEL CR4 — PLAIN ROLLERS



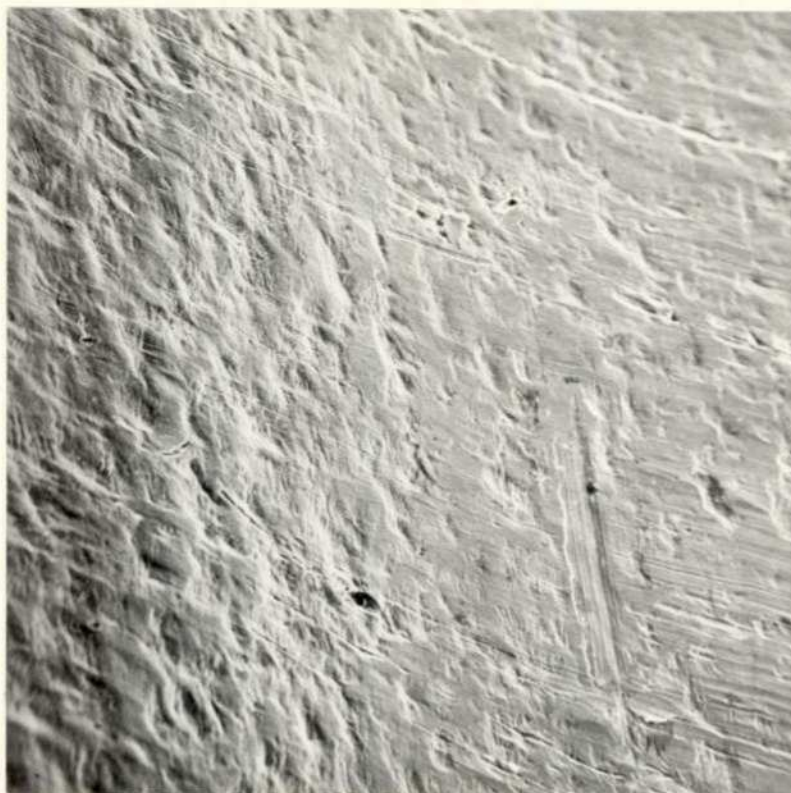
(a) x 500

Formation of groove

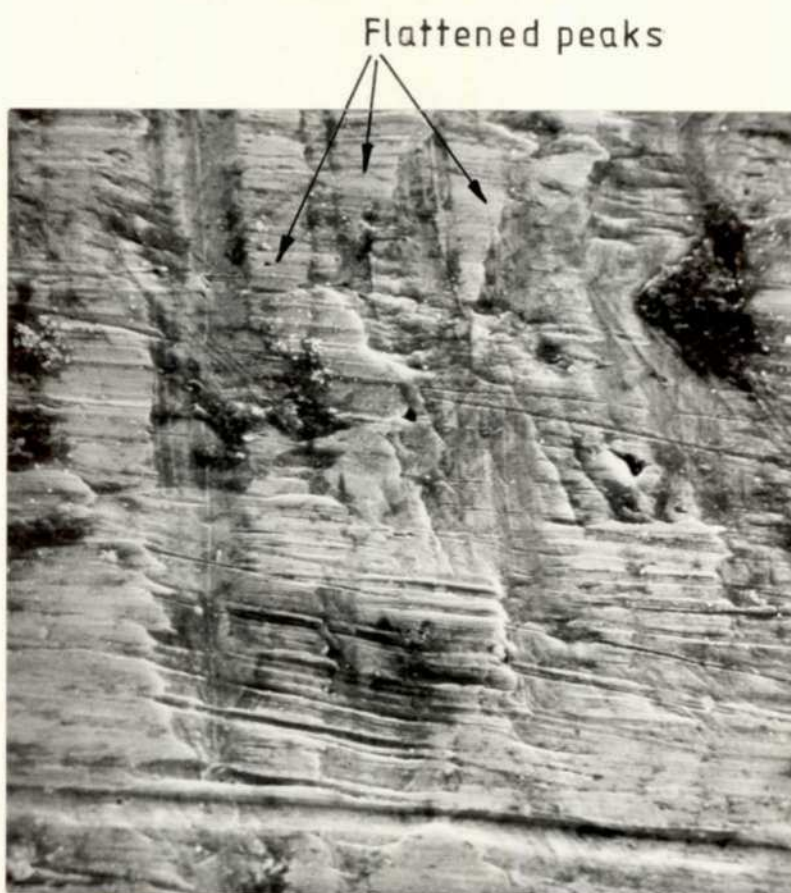


(b) x 2000

FIGURE 38: SCANNING ELECTRON MICROGRAPH
MILD STEEL CR4 — KNURLED ROLLERS

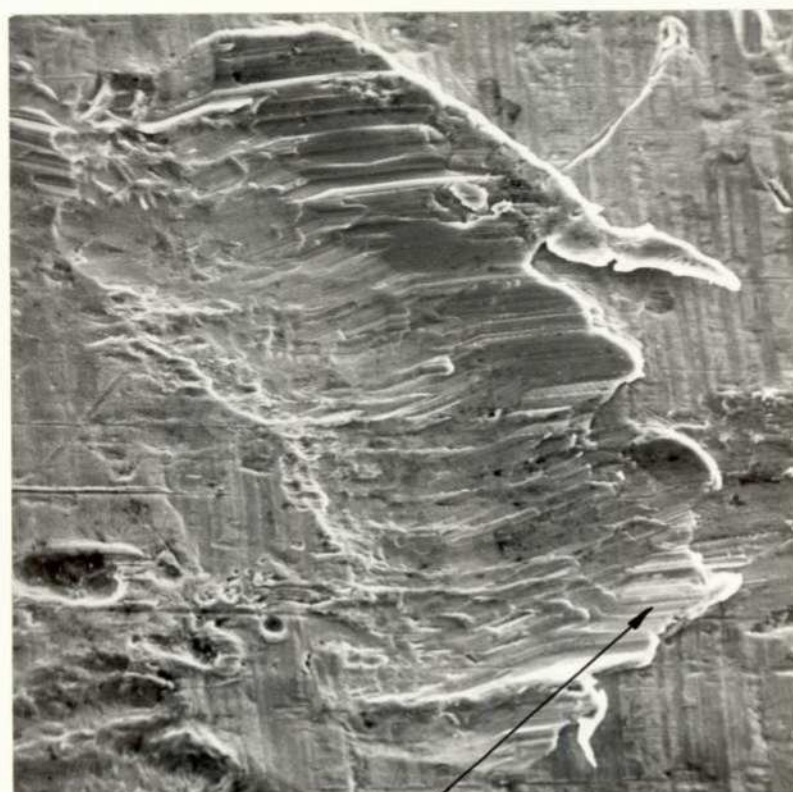


(a) x 50



(b) x 500

FIGURE 39: SCANNING ELECTRON MICROGRAPH
ALUMINIUM SIC — PLAIN ROLLERS



(a) x 200

Prow formation



(b) x 500

FIGURE 40: SCANNING ELECTRON MICROGRAPH
ALUMINIUM SIC—KNURLED ROLLERS

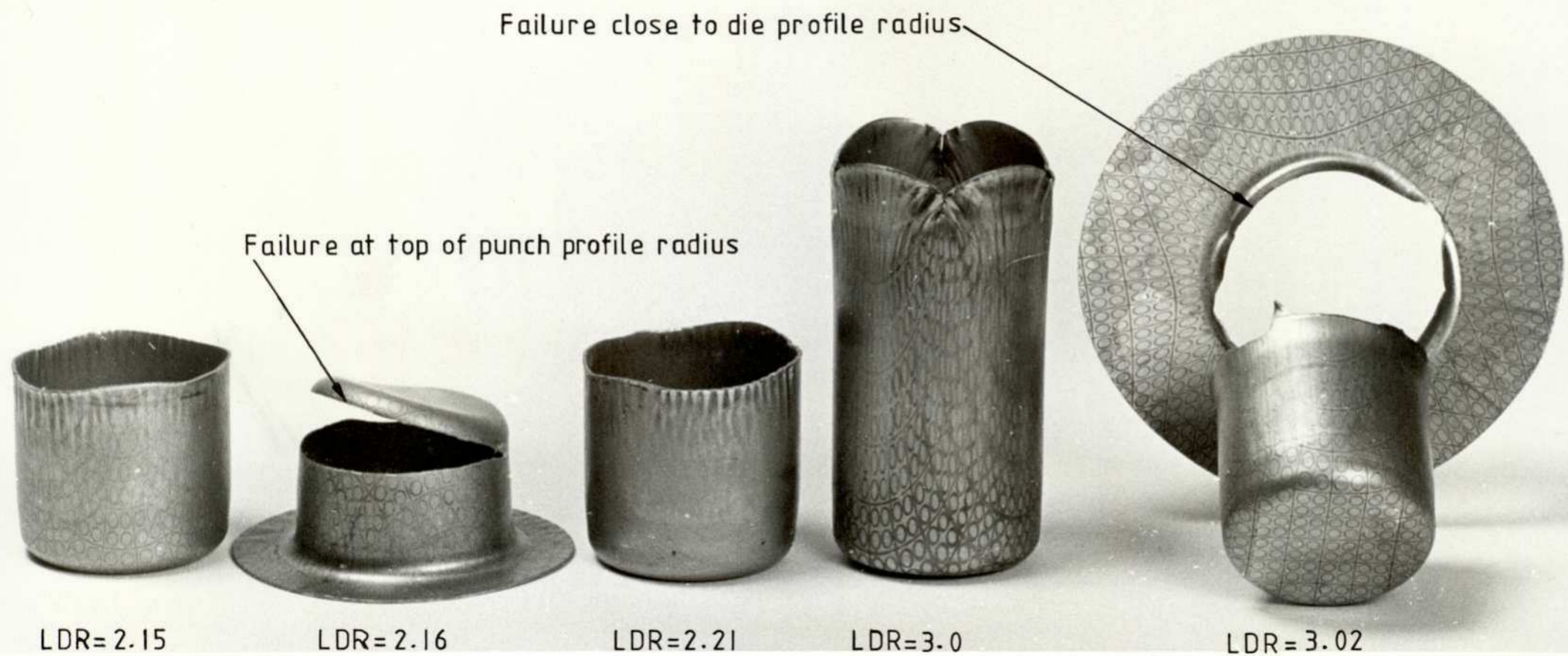


FIGURE 41: INCREASE IN LDR AND FAILURE MODES — STAINLESS STEEL En58B

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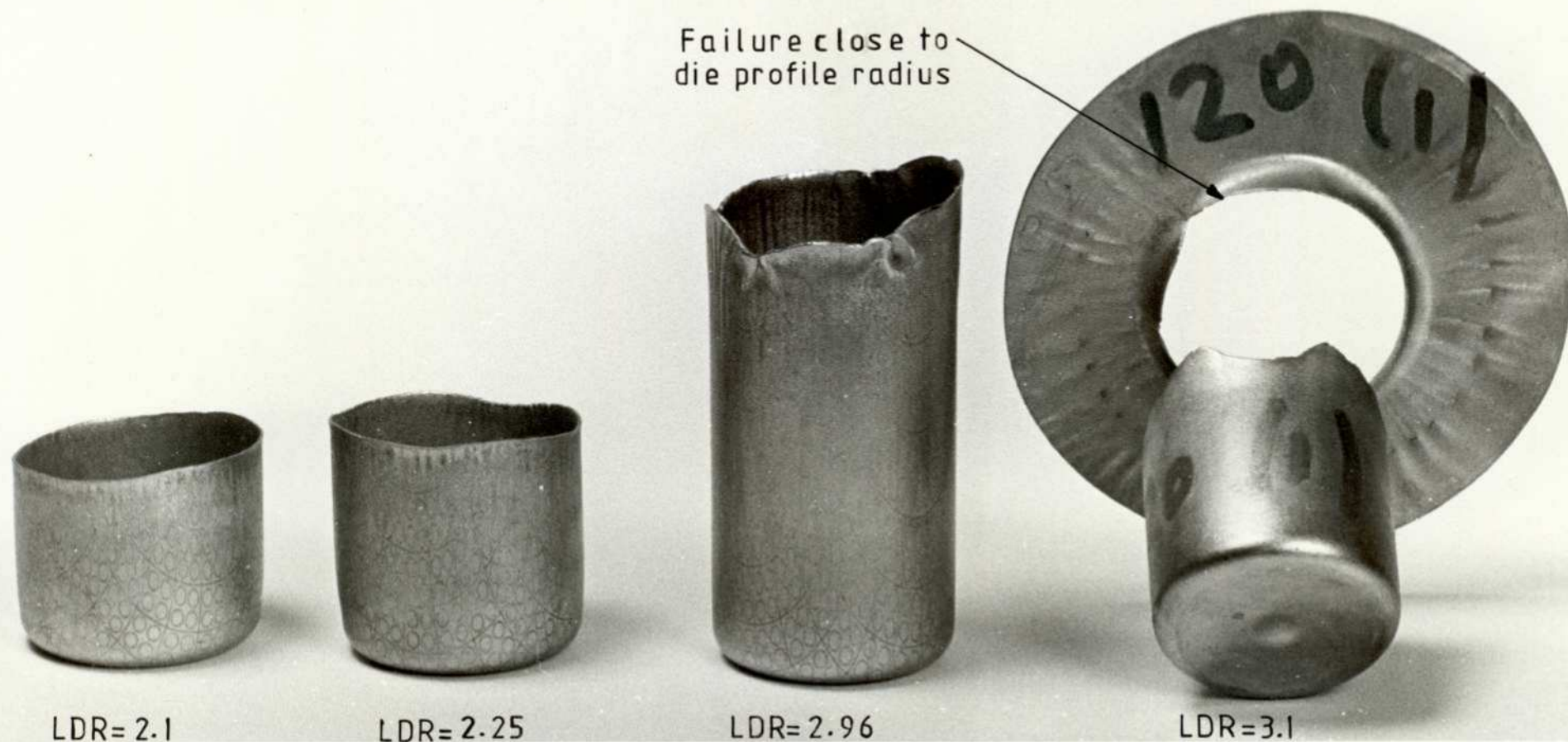


FIGURE 42: INCREASE IN LDR AND FAILURE MODES — 70/30 BRASS

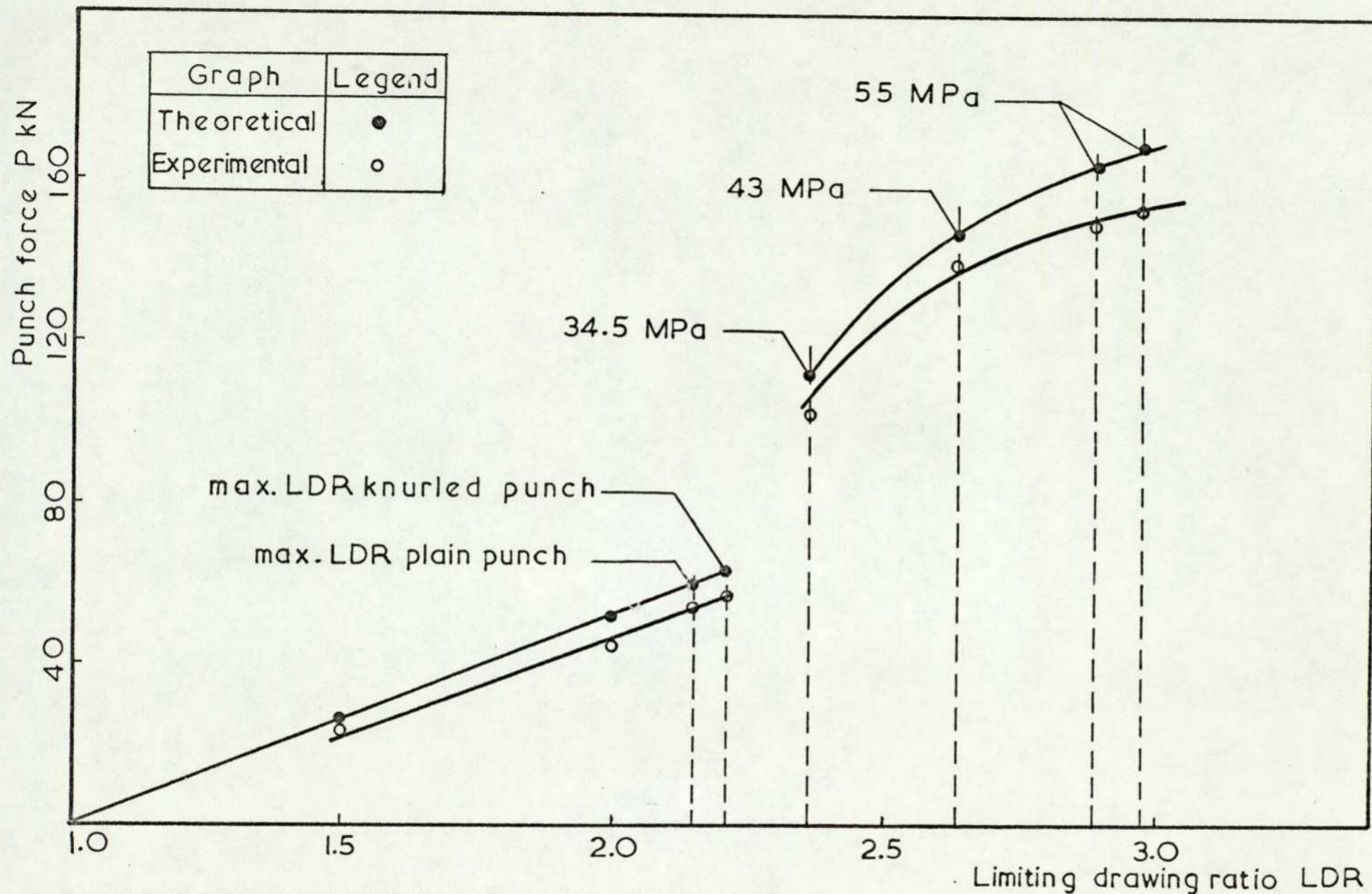


FIGURE 43: THEORETICAL AND EXPERIMENTAL VALUES OF PUNCH FORCE FOR STAINLESS STEEL En58B

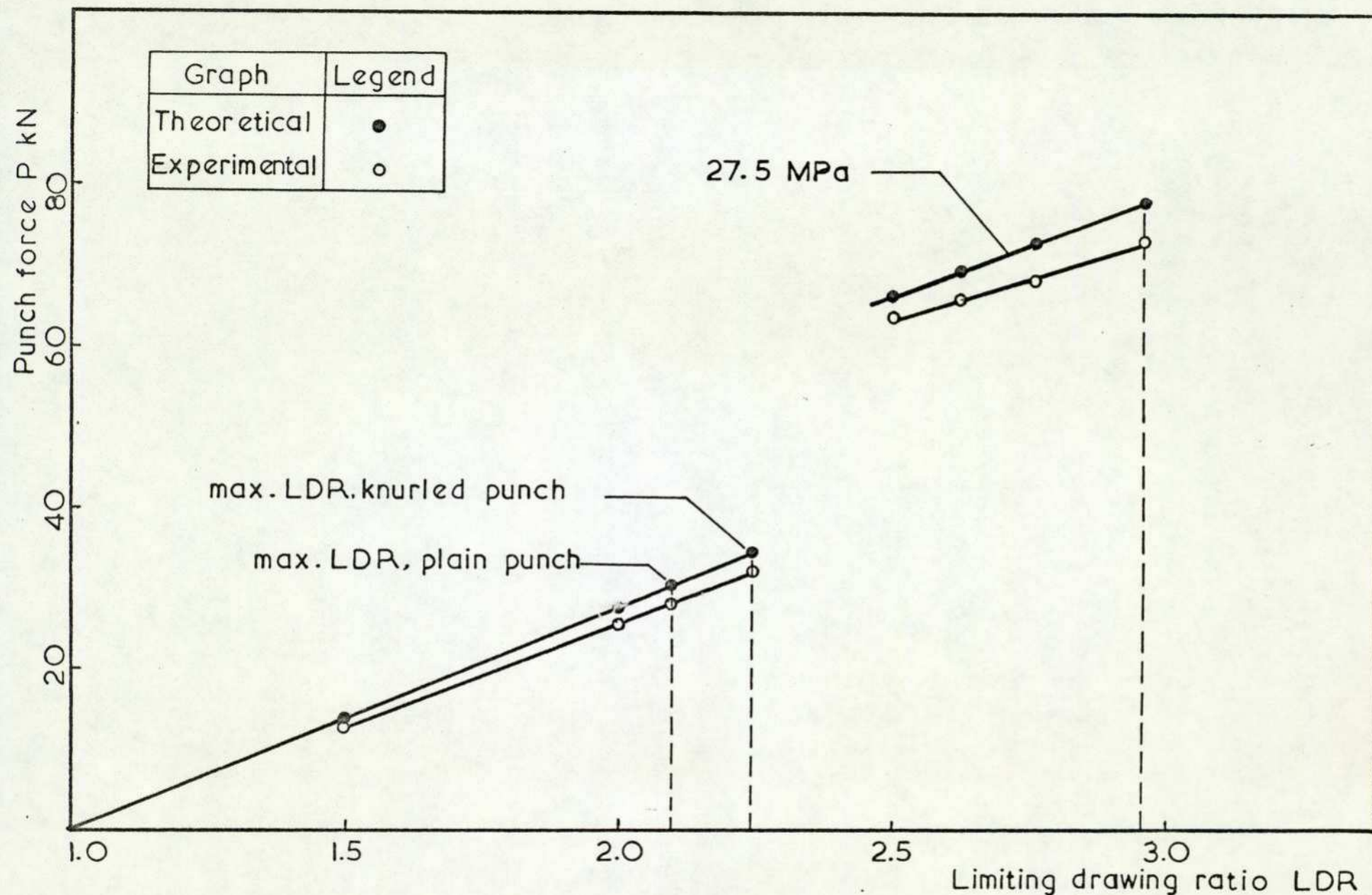
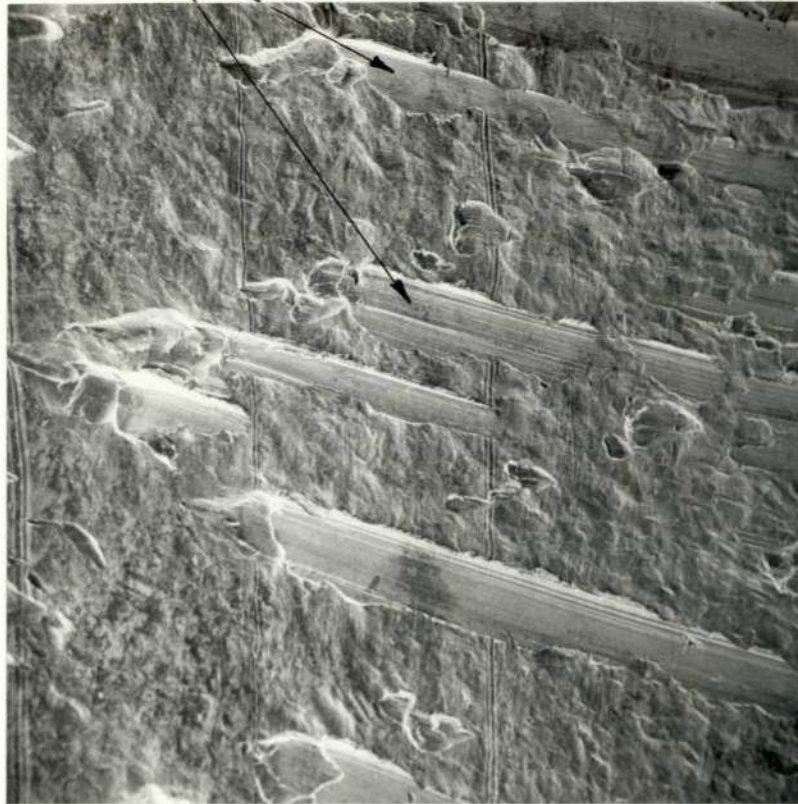


FIGURE 44: THEORETICAL AND EXPERIMENTAL VALUES OF PUNCH FORCE FOR 70/30 BRASS

Knurls having failed to hold



(a) x 20: LDR= 2.22, no pressure

Well defined knurl pattern



(b) x 20: LDR= 2.89, with pressure

FIGURE 45: SCANNING ELECTRON MICROGRAPH OF
BASE OF CUP-70/30 BRASS

Deep grooving



(a) x 20: LDR=2.84, with pressure

Original surface



(b) x 500: LDR=2.84, with pressure

FIGURE 46: SCANNING ELECTRON MICROGRAPH OF
BASE OF CUP—STAINLESS STEEL En58B

Limited penetration of knurls



(a) x20: LDR= 2.27, no pressure

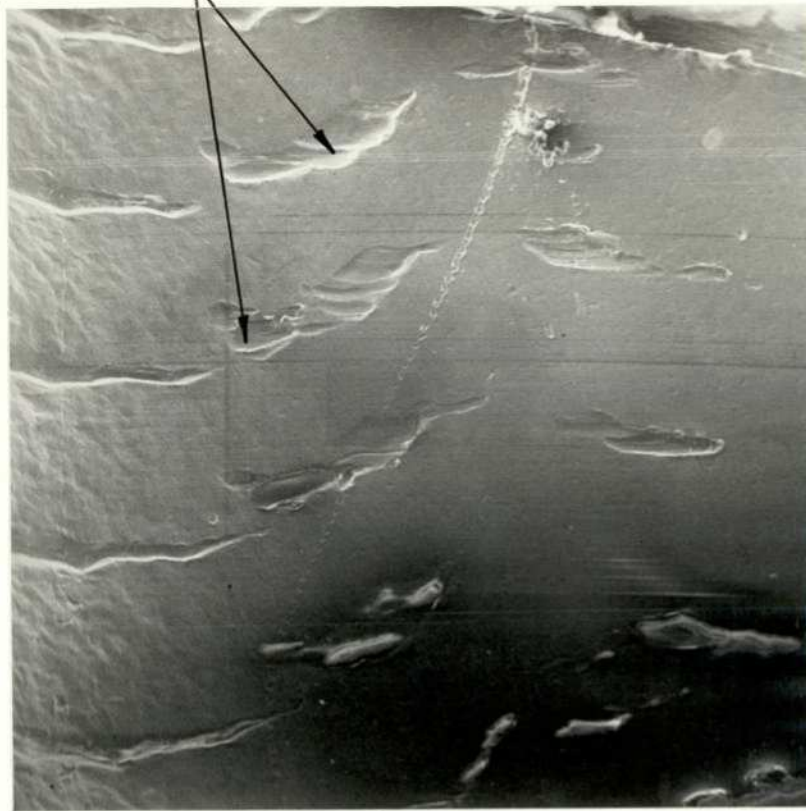
Deeper penetration of knurls



(b) x 20: LDR= 2.27, with pressure

FIGURE 47: SCANNING ELECTRON MICROGRAPH OF
BASE OF CUP—MILD STEEL CR4

Deep penetration of knurls



(a) x20: LDR=1.98, no pressure

Severe deformation and tearing



(b) x 50: LDR=1.98, with pressure

FIGURE 48: SCANNING ELECTRON MICROGRAPH OF
BASE OF CUP—ALUMINIUM SIC