



City Research Online

City St George's, University of London

Citation: Joorabchian, A. (1976). Spin-forging of sheet metal cones.
(Unpublished Doctoral thesis, The City University)

This is the accepted version of the paper.

This version of the publication may differ from the final published version. To cite this item please consult the publisher's version.

Permanent repository link: <https://openaccess.city.ac.uk/id/eprint/38288/>

Copyright and Reuse: Copyright and Moral Rights remain with the author(s) and/or copyright holders. Copies of full items can be used for personal research or study, educational, or not-for-profit purposes without prior permission or charge, unless otherwise indicated, provided that the authors, title and full bibliographic details are credited, a hyperlink and/or URL is given for the original metadata page and the content is not changed in any way. For full details of reuse please refer to [City Research Online policy](#).

THE CITY UNIVERSITY

DEPARTMENT OF MECHANICAL ENGINEERING

SPIN-FORGING OF SHEET METAL CONES

by

ALI JOORABCHIAN M.Sc.

A thesis submitted to The City University
as part of the requirements for the
degree of Doctor of Philosophy

NOVEMBER 1976

CONTENTS

	Page	
SUMMARY	1	
ACKNOWLEDGEMENTS	3	
LIST OF FIGURES	4	
NOTATION	7	
<u>CHAPTER 1</u>	INTRODUCTION	9
<u>CHAPTER 2</u>	REVIEW OF PREVIOUS RELEVANT WORK	16
<u>CHAPTER 3</u>	THEORETICAL CONSIDERATIONS	24
3.1	Forces in spin-forging	25
3.2	Uniform plastic deformation solution	27
3.3	Upper-bound estimate assuming plane strain deformation	31
3.4	Upper-bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a median plane	34
3.5	Upper-bound estimate assuming axisymmetric deformation and curved surfaces of tangential velocity discontinuity on a median plane	35
<u>CHAPTER 4</u>	DESCRIPTION OF THE EXPERIMENTAL EQUIPMENT	38
4.1	Centre lathe with hydraulic copying attachment	39
4.2	Rollers used for spin-forging	39
4.3	Mandrel adapter and replaceable conical mandrel heads	41
4.4	The three component force dynamometer	41
4.5	Templates	42
4.6	Specimen materials	43
4.7	Calibration equipment	43
4.8	Lubricant	44

CHAPTER 5

	EXPERIMENTAL PROCEDURE	46
5.1	Preliminary tests	47
5.2	Calibration of the three component force dynamometer	47
5.3	Determination of the tangential, radial and axial component forces at varying mandrel speed and constant feed rate	50
5.4	Determination of the tangential, radial and axial component forces at varying feed rate and constant mandrel speed	53
5.5	Determination of the tangential, radial and axial component forces using mandrels having various cone semi-angles	53
5.6	Determination of the tangential, radial and axial component forces at varying fractional reduction in wall thickness of formed cones having a constant cone semi-angle of 35°	54
5.7	Determination of the tangential component force using mandrels having various cone semi-angles at varying mandrel speed	54
5.8	Determination of the tangential component force using mandrels having various cone semi-angles at varying feed rate	55
5.9	Determination of the quasi-static true compressive stress-compressive natural strain curves for commercially pure aluminium and 70/30 brass at ambient temperature	56

CHAPTER 6

	RESULTS AND DISCUSSION	59
6.1	Effect of mandrel speed on the three component forces during the spin-forging operations at constant feed rate	60
6.2	Effect of feed rate on the three component forces during spin-forging operations at constant mandrel speed	63
6.3	Relation between the three component forces and the mandrel cone semi-angle at constant mandrel speed and feed rate	65

		Page
6.4	The dependence of the three component forces on the fractional reduction in wall thickness during spin-forging operations with a constant cone semi-angle of 35° and constant mandrel speed and feed rate	67
6.5	Relation between the tangential component force and mandrel speed using mandrels having various cone semi-angles	70
6.6	Relation between the tangential component force and feed rate using mandrels having various cone semi-angles	71
6.7	Uniform plastic deformation energy method for determination of the dimensionless tangential component force during the spin-forging of cones	72
6.8	Upper-bound estimate for the dimensionless tangential component force during the spin-forging of cones	74
<u>CHAPTER 7</u>	CONCLUSIONS AND RECOMMENDATIONS	79
7.1	Conclusions	80
7.2	Recommendations for future work	82
REFERENCES		85
BIBLIOGRAPHY		89
APPENDICES		
1	Uniform plastic deformation energy solution	91
2	Upper-bound estimate for the dimensionless tangential component force during the spin-forging of a cone assuming plane strain deformation	98
3	Upper-bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a medial plane	103
4	Upper-bound estimate assuming axisymmetric deformation and curved surfaces of admissible velocity field	109
5	Computer program for the optimum angle of ϕ	119

SUMMARY

This thesis examines the effects of some process variables on the performance and their relation with the various component forces during the spin-forging of cones from two sheet metals. Some of the theoretical methods available for the analysis of metal-forging problems have been used to determine the various forming forces involved in the process.

The spin-forging operation was performed on a robust centre lathe and cones were produced from commercially pure aluminium and 70/30 brass sheet. The experimental work performed to examine the various process variables can be classified to two groups:

- (a) using a mandrel of constant cone angle,
- (b) using mandrels with various cone angles.

The parameters examined with a mandrel of constant cone angle were, the effect of speed, feed rate and fractional reduction in wall thickness of the formed conical workpiece on the component forces during the spin-forging operations.

The parameters examined with a mandrel adapter capable of accepting different conical heads were, the effect of cone semi-angle on the component forces and the speed and feed rate on the tangential component force at various cone semi-angles during the spin-forging operations.

This study enables a judicious choice of the process variables mentioned above to be made as far as the forming forces or the power is concerned for a particular application. It is shown that the three component forces in spin-forging vary inversely as the mandrel speed and the cone semi-angle but vary directly as the feed rate. It is also concluded that an *optimum* mandrel speed exists for a given cone angle at constant feed rate and it varies directly proportional to the cone semi-angle and increases with feed rate for the range of feed rate examined. It is further shown that the component forces are dependent on the final thickness of the formed workpiece and the condition of spin-forging.

Experimental values of the tangential component force as a function of the fractional reduction in thickness of the formed cones are compared with those predicted by the elementary theories and the uniform plastic deformation method.

Upperbound estimates for the tangential component force are presented assuming plane strain deformation and axisymmetric deformation assuming both plane and curved surfaces of velocity discontinuity. These theoretical predictions are compared with the experimental values for varying fractional reduction in thickness of the cones. The Upperbound estimate assuming plane strain deformation provides the best correlation with the experimental results.

ACKNOWLEDGEMENTS

The author would like to express his thanks to Professor J. C. Levy, Head of Department of Mechanical Engineering, The City University, for making it possible to carry out this work.

He is also most grateful to ■■■ ■■■■■, Managing Director of Korosh Industrial Manufacturing Group, Tehran, Iran, for taking a personal interest in this study and also for providing the financial support.

The author is deeply indebted to ■■■ ■■■■■ for his patient, skillful and sympathetic guidance throughout all the stages of this research.

Sincere thanks are offered to the technician staff of the University Central Workshop for preparing the specimens required for spin-forging and the technical assistance provided by ■■■ ■■■■■ of the Manufacturing Processes Laboratory.

Thanks are also due to ■■■■■ ■■■■■ who was responsible for typing this thesis.

LIST OF FIGURES

Figure No	Description	Page No
1	Schematic diagram of spinning and spin-forging	122
2	Force diagram for spin-forging of cones	123
3	Approximate area of contact between the roller and the cone	124
4	Arrangement of experimental equipment	125
5	Close up view of the roller, mandrel and template	126
6	The different shap of roller	127
7	Mandrel and assembled roller	128
8	Arrangement of calibration equipment	129
9	Calibration graph loading in z direction	130
10	Calibration graph loading in y direction	131
11	Calibration graph loading in x direction	132
12	Calibration graph loading in centre	133
13	True compressive stress-natural strain curve for commercially pure aluminium	134
14	True compressive stress-natural strain curve for 70/30 brass	135
15	Relationship between component forces and mandrel speed for commercially pure aluminium	136
16	Relationship between component forces and mandrel speed for 70/30 brass	137
17	Relationship between tangential component force and mandrel speed at various feed rate for commercially pure aluminium	138
18	Relationship between tangential component force and mandrel speed at various feed rate for 70/30 brass	139

Figure No	Description	Page No
19	Relationship between component forces and feed rate for commercially pure aluminium	140
20	Relationship between component forces and feed rate for 70/30 brass	141
21	Relationship between component forces and cone semi-angle for commercially pure aluminium	142
22	Relationship between component forces and cone semi-angle for 70/30 brass	143
23	Relationship between component forces and fractional reduction in wall thickness for commercially pure aluminium	144
24	Relationship between component forces and fractional reduction in wall thickness for 70/30 brass	145
25	Wrinkling of the flange	146
26	Relationship between tangential component force and mandrel speed at various cone semi-angles for commercially pure aluminium	147
27	Relationship between tangential component force and mandrel speed at various cone semi-angles for 70/30 brass	148
28	Relationship between optimum speed and cone semi-angle at different feed rate for commercially pure aluminium	149
29	Relationship between optimum speed and cone semi-angle at constant feed rate for 70/30 brass and commercially pure aluminium	150
30	Relationship between tangential component force and feed rate at various cone semi-angle for commercially pure aluminium	151
31	Relationship between tangential component force and feed rate at various cone semi-angle for 70/30 brass	152
32	Comparison of the dimensionless tangential component force predicted by uniform deformation energy method and the experimental values for commercially pure aluminium	153
33	Comparison of the experimental values of dimensionless tangential component force with the elementary theories and upper bound estimate assuming plane strain deformation	154

Figure No	Description	Page No
34	Comparison of various upper bound estimates and experimental values for commercially pure aluminium	155
35	Comparison of various upper bound estimates and experimental values for 70/30 brass	156
A-1	Cylindrical co-ordinate system	157
A-2	Physical diagram and corresponding hodograph for upper bound estimate assuming plane strain deformation	158
A-3	Physical diagram for upper bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a median plane	159
A-4	Physical diagram and hodograph for upper bound estimate assuming axisymmetric deformation and curved surfaces of admissible velocity field	160

NOTATION

Symbol	Description
X, Y, Z	Rectangular co-ordinate system
O	Origin
r, θ, Z	Cylindrical polar co-ordinate system
A_t, A_r, A_a	Projected area of contact surface between roller and cone in tangential, radial and axial directions respectively
t_o	Initial wall thickness of the blank
t_f	Final wall thickness of the workpiece
α	The mandrel cone semi-angle
F_t	Tangential component force
F_r	Radial component force
F_a	Axial component force
f	Feed per revolution
N	Speed of rotation of the mandrel
r_o	"Round off" radius of the roller
r	Radius of the blank
R	Fractional reduction in wall thickness
$\dot{W}$	Total power consumed
$\bar{\sigma}$	Average flow stress
σ_m	The hydrostatic stress
$\sigma_r, \sigma_\theta, \sigma_z$	Components of the normal stress
$\tau_{rz}, \tau_{z\theta}, \tau_{r\theta}$	Components of the shear stress
$\bar{\epsilon}$	Effective strain
$d\bar{\epsilon}$	Infinitesimal effective strain

Symbol	Description
$\int d\bar{\epsilon}$	Total effective strain
$\dot{\bar{\epsilon}}$	Effective strain rate
$\dot{\epsilon}_r, \dot{\epsilon}_z, \dot{\epsilon}_\theta$	Direct strain rate components
$\dot{\gamma}_{rz}, \dot{\gamma}_{r\theta}, \dot{\gamma}_{z\theta}$	Shear strain rate components
P	Normal stress acting on the contact surfaces
k	Yield shear stress of the material
V_t	Circumferential velocity of the cone
V_r	Radial velocity of the cone
V_a	Axial velocity of the roller
$\dot{\gamma}$	Maximum shear strain rate
$\bar{V}^*$	Absolute value of velocity discontinuity
dv	Volume element
ds	Line element length of the velocity discontinuity
β	Angle of inclination of the land
ϕ	Angle of inclination to the axis of exit line
θ_r	Angle of inclination to the axis of entry line
U_r	Velocity in r direction
U_θ	Velocity in θ direction
U_z	Velocity in z direction
η	Efficiency

CHAPTER ONE

INTRODUCTION

INTRODUCTION

Spin-forging is a metal-forming process which has been developed during recent years from the metal spinning process widely used to fabricate sheet metal components having rotational symmetry. This relatively new technique referred to here as spin-forging has also been referred to as, for example, flow turning, "Hydrospin", power spinning and shear spinning. However, some of these terms tend to be ambiguous since the actual process is not intended to involve metal removal as in conventional turning and it should also be noted that there is a distinction between the processes of spinning and spin-forging. In common metal spinning, which was originally a craft operation, the sheet metal is clamped between a pattern or mandrel and a friction block and while the pattern, blank and clamping members are revolving the blank is forced over the shaped mandrel by means of a spinning tool and the compressive force required had to be controlled carefully by hand to avoid thinning or cracking of the metal. During the spinning process, usually the initial wall thickness of the material, t_0 , remains unchanged, while in spin-forging operations the final thickness, t_f , of the finished cone is dictated by the sine of the cone semi-angle, α , as

illustrated in figure 1. In true spin-forging

$$t_f = t_o \sin \alpha$$

Since there is not an appreciable change in the radial position of each element from its original position in the circular blank, the maximum diameter of the formed cone thus remains approximately the same as the original blank diameter when the whole length of the circular flange is converted to the finished cone.

Two common spin-forging processes are now available in manufacturing industry, namely, the spin-forging of cones, and spin-forging of tubular products. This study is only concerned with the spin-forging of cones from sheet metal. The process of spin-forging is characterised by the plastically deforming zone being localised to a very small region of the workpiece in contact with a forming roller thus reducing the forming force required to effect a given deformation. Smaller machines may therefore be used for suitable spin-forging applications.

The machine used for the spin-forging of relatively small components up to 500 mm long and 300 mm diameter is basically a lathe with the main slides

being hydraulically actuated and also equipped with a hydraulic copying attachment and a hydraulic clamping unit in the tailstock for approximate positioning of the blank against the mandrel. These machines are usually versatile with a wide range of applications, and most metals can be spun successfully on them. Aluminium, copper, brass and steel are the most common materials which can be spun with excellent results.

A main advantage making this process suitable for many applications is an improvement in mechanical properties of the workpiece material, that is, an increase in hardness and ultimate tensile strength. However, this is usually accompanied by a decrease in ductility. Other advantages are speed of operation, little waste and economy, especially when manufacturing parts from expensive materials. Also for smaller batch production, the tooling and setting requirements are comparatively simple and relatively inexpensive. Some of the mandrels or patterns used in making prototypes could be made from wood or the softer metals and they can be produced by simple turning and machining operations which do not require a highly skilled

tool and die maker. Some important applications of this process are the manufacture of conical parts to a high degree of dimensional accuracy and close control of tolerances which include, for example, radar reflectors, components for jet and turbine engines, satellite nose cones, rocket components, truck wheel rims and parts for nuclear reactors.

With the emergence of automatic techniques during the last twenty years or so, attempts have been made to automate the spinning process and more recently, spin-forging. In order to design the required control system and complicated hydraulic machinery, a prediction of the force, energy and power requirement as well as a knowledge of the nature of the deformation process is essential. For this reason, the aim of this study is directed towards obtaining a better understanding of the process of spin-forging of cones from a sheet metal to enable the appropriate process variables for a specific application to be selected and also to develop a useful method for prediction of the forces involved during the operation. The analysis of the spin-forging process has not received a great

deal of attention, particularly in recent years, and no attempt appears to have been made to apply the upper-bound theorem to obtain an estimate of the forming parameters by assuming, for example, either plane strain or axisymmetric deformation. It is worth noting that the previous research work on spin-forging is either mainly concerned with the theoretical aspect of the process associated with little experimental data or, alternatively, with the results of experimental work only.

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

- (a) to contribute to knowledge in the field of spin-forging which has not been fully explored to date,
- (b) to present the results of an experimental study concerned with the effect of the mandrel speed, feed rate, fractional reduction in thickness and cone semi-angle on the components of the resultant force exerted at the workpiece-roller interface during the spin-forging of cones from sheet metal and
- (c) to give tentative upper-bound estimates for the tangential component force assuming

(i) plane strain deformation

and

(ii) axisymmetric deformation assuming both plane and curved surfaces of velocity discontinuity and to compare the theoretical predictions with the experimental results.

CHAPTER TWO
REVIEW OF PREVIOUS
RELEVANT WORK

REVIEW OF PREVIOUS RELEVANT WORK

In the last twenty years or so, extensive development work has taken place in relation to the process of spinning by attempting to automate the process and replace the operator with mechanical power. The original spinning process involved a great deal of skill and experience on the part of the operator and the choice of material and its dimensions were largely limited by the strength of the operator. In some operations a number of intermediate steps were required to avoid any fracture or bulking due to local deformation and the tool had to be moved backwards and forwards by hand over the workpiece many times until the material was flatly laid on the pattern or mandrel. This development work on spinning has resulted in the new technique of *spin-forging* which has the following advantages over common spinnings;

- (a) deformation is completed in one pass of a roller,
- (b) mandrel speed, feed rate, thickness reduction and the pressure of the roller on the material is selected before starting the operation,
- (c) a very highly skilled operator is not required,
- (d) better dimensional accuracy and surface finish can be obtained,
- (e) larger and heavier parts can be produced because of the extra power available and finally,
- (f) improvement of mechanical properties often eliminate the need for subsequent heat-treatment.

The literature available before the development of spin-forging is not very relevant in the context of the present study except that it presents the art of spinning and the history of the process. The advancement of spin-forging was originated by realising that the process is

near to point-deformation and by localising the plastically deforming zone to a small region of the workpiece it is possible to reduce the required forming forces significantly. Examples of other processes involving similar progressive plastic deformation which consequently have some features similar to that of spin-forging, include the helical rolling process [1], rotary piercing [2], ring rolling [3] and rotary forging [4], [5]. Kienzle [6] has discussed this more recent trend in relation to spin-forging and stated that the reduction in forming force required is due to the fact that the area of contact between the roller and workpiece is reduced considerably and consequently the size of the machine required to effect a given deformation is reduced for such processes.

The existing literature in this field is mainly concerned with analytical aspects of the process and the common factors for most of these studies that try to analyse the stresses and strains assume a von Mises material and use the deformation theory to express the stress-strain relations. Also, of the forming forces, the tangential component force, F_t , has been given more attention in the analytical considerations than the other two forces, namely, radial, F_r , and axial, F_a , components of the resultant force exerted at the workpiece-roller interface. This is obvious since the tangential component force together with the mandrel speed and feed rate determine the power required at the mandrel to perform a particular spin-forging operation. However, information relating to the radial and axial component forces becomes essential when designing the driving mechanism and roller assembly.

One of the early solutions for the tangential component force, F_t ,

during spin-forging of cones from sheet metal is given by Paulton and Colding [7]. These authors start by discussing the features of the process and derive an equation to predict approximately the tangential component force and power which is based essentially on the concept of the energy dissipation during a homogeneous plane strain deformation. Their equation for the tangential component force takes the form,

$$F_t = (A_a/\eta) \bar{\sigma} \bar{\epsilon} \quad (2)$$

where, A_a , is the projected area of contact between the workpiece and roller in the axial plane and is shown to be given by $A_a = R t_o f$,

$R = (t_o - t_f)/t_o$, is the fractional reduction, t_o , the original blank thickness and, f , the feed per revolution of the mandrel.

Assuming $\eta = 1$, equation (2) can therefore be restated as,

$$F_t/\bar{\sigma} t_o f = R \bar{\epsilon} = R \ln(t_o/t_f) \quad (3)$$

where, $\bar{\sigma}$, is the average flow stress, $\bar{\epsilon}$, is referred to as an effective strain given by $\ln(t_o/t_f)$ and, t_f , is the final thickness of the finished cone. The average flow stress, $\bar{\sigma}$, was determined from uniaxial tensile test results using the Bridgman correction [8] to account for the triaxial stress state which occurs after the condition of instability in uniaxial tension is reached and a neck is formed in the specimen. In order to estimate the value of, F_t , it is necessary to determine the efficiency value of, η , which is suggested would have a magnitude of approximately 50-60 per cent under normal conditions of deformation.

It is also shown that the magnitude of this efficiency, η , depends on the mandrel speed, feed rate, lubrication and material thickness.

Equation (2) predicts a tangential component force leading to energy and power values which are very low, since the effective strain, $\bar{\epsilon}$, employed in the equation is an underestimate and also the effect of redundant plastic deformation during the process is neglected.

Kalpakioglu [9] has analysed the spin-forging of cones and presented a solution for predicting the tangential component force based on the assumption that the process is one of simple shear deformation under the roller. He also simplifies the analysis for the nature of deformation by using an ideal model which assumes that the forming of the flange may be likened to the sliding of a deck of cards over each other. This flow pattern was supported by a grid-line analysis of the process which showed that axial lines in the blank remained essentially axial in the spun parts. His analysis also utilises the energy method and the tangential component force, F_t , is given by,

$$F_t = t_o \cdot f \cdot \sin \alpha \int_0^{\bar{\epsilon}} \bar{\sigma} d\bar{\epsilon} \quad (4)$$

where, f , is the feed per revolution of the mandrel, t_o , is the initial thickness of the blank α , is the mandrel cone semi-angle and $\bar{\sigma}$ is the effective stress. The integral $\int_0^{\bar{\epsilon}} \bar{\sigma} d\bar{\epsilon}$ defines the area under an effective stress-strain curve for the deforming material up to a value of effective strain given by $\bar{\epsilon} = \cot \alpha / \sqrt{3}$.

Assuming a mean value of the effective stress $\bar{\sigma}_m$ for a given material then equation (4) can be restated as;

$$F_t / \bar{\sigma}_m t_o f = \sin \alpha \bar{\epsilon} = \sin \alpha \cot \alpha / \sqrt{3} = \cos \alpha / \sqrt{3} . \quad (5)$$

The effective strain, $\bar{\epsilon}$, used in equation (4) does not include redundant and any other straining and is based entirely on a simple shear process. Hence the value of tangential component force, F_t , predicted by equation (4) will again be an underestimate. By using an idealised model for the spin-forging process, he determined expressions for the shear strains, shear strain rate and specific energy and compared these with

the experimental data. Kalpakcioglu [10] also experimentally studied the plastic deformation during the spin-forging of cones and tubes by various techniques and the best representation of the metal flow pattern was obtained by a grid-line method. However, he observed that the actual deformation is rather similar to a simple shear process and in addition there is considerable circumferential flow. Near the mandrel the axial lines are distorted and tend to become normal to the mandrel surface. In another paper [11] the same author studied the spinnability of the metal and discussed the factors which influence the formability and the properties required to withstand the stress and strain imposed on the metal during the operation.

Avitzur and Yang[12] have used a more sophisticated analysis for determining the strain rates in the deforming material from the analysis of the velocity field under the roller during the operation. They derived an equation for predicting the tangential component force which is of a complex nature and requires the aid of a computer for its solution. However, they neglect the energy dissipation due to redundant deformation which could be significant at lower values of feed rate when the roller traverses a number of times over the same region of the metal.

Kobayashi, Hall and Thomson [13] offer another method for the analysis of spin-forging of cones and derive a similar solution to equation (4) for predicting the tangential component force, F_t , except that their total effective strain includes the strain due to bending of the flange under the roller. Their equation for the tangential component force takes the form,

$$F_t = t_o \cdot f \cdot \sin(\alpha) \cdot \bar{\sigma}_m \int d\bar{\epsilon} \quad (6)$$

where, $\bar{\sigma}_m$, is the average effective stress determined from a frictionless compressive stress-strain curve and, $\int d\bar{\epsilon}$, is the total effective strain due to simultaneous bending and shearing of the element under the roller. However, no allowance is made for redundant deformation or the effects of strain rate and temperature on the flow stress of the deforming material during a spin-forging operation. Nevertheless, the correlation between predicted values of the tangential component force, F_t , and the experimental results given appears to be surprisingly close for commercially pure aluminium and pure lead sheet metal cones. These authors also derived equations for obtaining the radial, F_r , and axial, F_a , component forces from the tangential component, F_t , and the ratio of their appropriate area of contact between the workpiece and roller.

Kegg [14] describes a new test method for determining the spinnability of metal which is the ability to undergo a spin-forging deformation without fracture by using an ellipsoidal mandrel. The author comes to the conclusion on the basis of his test results, that the spinnability of most metals may be predicted from the value of their reduction in area obtained from tensile test results. Kalpakcioglu [11] has discussed the Kegg spinnability tests and among others, comes to the following conclusions:

- (1) The roller corner radius, roller swivel, roller velocity and mandrel speed do not appear to influence the spinnability of metals.
- (2) Variation from the sine-law thickness reduction is a major factor influencing spinnability.

(3) It is shown experimentally that the minimum cone semi-angle, α , for spin-forging of a cone from a sheet metal is about 15 degrees ($\alpha = 15^\circ$).

Younger and Foster [15] have recently discussed the power and the forming forces involved in spinning. They measured the power consumption and various tool forces during a spinning process of aluminium and mild steel sheet metal using the two different tool forms. They only considered the two component forces in the tangential and axial direction and stated that, since in spinning operations the wall thickness of the workpiece remains unchanged, the radial component force becomes insignificant. They finally came to the following conclusions,

- (1) The motor power consumption in spinning is directly related to the tangential component force on the tool.
- (2) The axial force on the tool reaches a maximum value in the early stage of spinning and then reverses direction in the later stages.
- (3) Tool form has a significant effect on the maximum positive axial force and the maximum diameter of disc that can be spun.

CHAPTER THREE

THEORETICAL
CONSIDERATIONS

3.1 Forces in spin-forging

In order to be able to express mathematically the energy required to produce the plastic deformation during spin-forging of cones from a sheet metal, a knowledge and understanding of the nature of the forming forces are essential. The forces of interest in spin-forging of cones are the tangential (F_t), radial (F_r), and axial (F_a) components of the resultant force exerted at the work piece-roller interface. Figure 2 shows a force diagram for spin-forging of cones where the axis of the roller is parallel to the side of mandrel. By using a three component force dynamometer, the magnitude of these component forces can be measured and of the three force components exerted on the roller, the tangential component force has the lowest and the radial component force has the highest magnitude. Nevertheless, it is the tangential component force which determines the power required at the headstock of the machine to perform a particular spin-forging operation. The other two component forces, F_r and F_a , which act in the normal and parallel directions relative to the roller axis respectively are important when designing the driving mechanism of the machine and also the roller. The magnitudes of the radial and axial component forces often determine the size of the roller required since the roller radius depends on the size of the roller bearings which have to be of higher load carrying capacity to withstand these component forces.

Since there has not been any direct analytical solution to predict the magnitude of the radial and axial component of the resultant force exerted at the workpiece-roller interface, an approximation for these component forces may be obtained from the ratio of their projected contact areas between roller and workpiece. The three component forces, F_t , F_r and F_a may be expressed as a function of normal stress, p , by assuming that the normal stress acting on the contact surface between roller and cone is uniformly distributed, then $F_t = p A_t$, $F_r = p A_r$ and $F_a = p A_a$ or in terms of F_t

$$F_r = F_t (A_r / A_t) \quad \text{and} \quad F_a = F_t (A_a / A_t) \quad (7)$$

where, p , is the normal stress acting on the contact surface between the roller and cone and A_t , A_r and A_a are the projected areas of contact surface between roller and workpiece in the tangential, radial and axial directions respectively. Considering figure 3, the approximate values of these areas of contact could be used to determine the ratio of tangential to axial component force during the spin-forging of cones. It can be seen from figure 3, that the projected areas of contact surface in the tangential and axial directions may be approximated by assuming these areas to be triangular such that

$$\left. \begin{aligned} A_t &= (1/2)R.R \cot \alpha , \\ A_a &= (1/2)f \cos \alpha (R/\sin \alpha) \end{aligned} \right\} \quad (8)$$

where, R , is the fractional reduction in wall thickness of the cone and, f , is the axial feed per revolution assumed to be unity.

Substituting for A_t and A_a from equation (8) into equation (7), the ratio of the tangential to axial component force is then given by,

$$F_t/F_a \approx \{(1/2)R^2 \cot\alpha\}/\{(1/2)R \cot\alpha\}$$

finally $F_t/F_a \approx R$ (9)

3.2 Uniform plastic deformation solution

The uniform deformation energy method is used to derive an expression for the average forming parameters from the work of plastic deformation. The main assumptions made in obtaining the expression for the power and the dimensionless tangential component force are;

- 1 - Material
- 2 - Process

- | | |
|---|---|
| 1(a) The material is homogeneous and isotropic. | 2(a) No frictional resistance between roller and workpiece. |
| 1(b) No strain hardening. | 2(b) Uniform deformation occurs. |
| 1(c) No elastic deformation and consequently no volumetric changes. | 2(c) No thermal effects. |
| 1(d) von Mises' yield criterion is applicable. | 2(d) Deformation under the roller is pure shear. |

The total infinitesimal deformation energy is expressed by,

$$W = \int_V (\bar{\sigma} d\bar{\epsilon}) dV \quad (10)$$

where, W is the total infinitesimal deformation energy, dV is the volume of the metal displaced across the boundary of the plastic deformation zone, $\bar{\sigma}$, and, $\bar{\epsilon}$, are effective stress and effective strain respectively. The, $d\bar{\epsilon}$, is the infinitesimal effective strain and determined from the velocity field under the roller. Since the thickness of the cone wall is small compared with the minimum radius of the cone, it can be assumed that the normal strains are approximately uniformly distributed.

The total rate of deformation energy during the spin-forging of cones is

$$\dot{W} = (2\pi/\sqrt{3}) \sigma_0 t_0 r N f \cos \alpha \quad (11)$$

where, σ_0 , is the flow stress, t_0 , initial thickness of the blank, r , is the radius of the blank, N , is the rotational speed of the mandrel and, f , is the feed/rev (see appendix 1).

The external power supplied is consumed in producing the plastic deformation through the interaction of force between the roller and cone. It is assumed that there is no power required to overcome frictional resistance at the workpiece-roller interface, since the circumferential velocities of roller and the cone are approximately equal. This assumption is supported

by the experiments conducted by the author which show that no appreciable changes are observed in the magnitudes of the spin-forging forces during both dry and lubricated conditions. The power consumed to cause the deformation can be composed of the total power delivered by the three component forces, namely the tangential, radial and the axial, in the absence of frictional forces. The power is given by the product of force and the velocity directed parallel to it then,

$$\dot{W} = \text{total power} = F_t V_t + F_r V_r + F_a V_a$$

where, F_t , F_r , and F_a , are components of the resultant force exerted at the workpiece-roller interface and V_t is the linear velocity of the cone ($V_t = 2\pi rN$), V_r , is the radial velocity ($V_r = 0$) and, V_a , is the axial velocity of roller $V_a = fN = \text{feed rate}$.

It can be assumed that the total power consumed during the deformation is due to the tangential component force alone, since the feed velocity (axial) is relatively small compared to the velocity of the mandrel. Although the magnitude of the axial component force is greater than the tangential component force the ratio of their velocities is too small to be considered very significant in power calculations. In a typical example where $f = \text{feed/rev} = 30 \times 10^{-3} \text{ cm / rev}$ and $r = \text{blank radius} = 10 \text{ cm}$ the ratio of the axial velocity to the cone velocity will be

$$V_a/V_t = fN/2\pi rN$$

substituting the values given above the ratio of axial velocity to cone velocity becomes

$$V_a/V_t = (30 \times 10^{-3})/(2\pi \times 10) = 4.8 \times 10^{-4}$$

Considering the ratio of the velocities obtained, it can be safely

assumed that the total power required during the deformation is that due to the tangential component force alone. Hence

$$\dot{W} = F_t V_t \text{ or } F_t = \dot{W}/V_t \quad (12)$$

Substituting for, $\dot{W}$, from equation (11) into equation (12) produces

$$F_t = \{ (2\pi/\sqrt{3}) \sigma_o t_o rN f \cos\alpha \} / (2\pi rN)$$

or
$$F_t = (1/\sqrt{3}) \sigma_o t_o f \cos\alpha$$

finally
$$F_t/t_o \sigma_o f = (1/\sqrt{3}) \cos\alpha \quad (13)$$

Using the approximate solution obtained above, it can be seen that it is quite easy to calculate the power and the tangential component force during the spin-forging of cones. The predicted values for tangential component force from equation (13) and the corresponding experimental values for varying fractional reduction in wall thickness, R , of the formed workpiece are given in figure 32.

By referring to figure 32, it will be seen that the predicted values given by equation (13) do not provide a good correlation with the experimental values and the shape of the curve is convex upwards whilst the experimental curve is convex downwards. An improved analytical solution is therefore necessary to give the correct characteristic shape for the theoretical values of the tangential component force.

3.3 Upper-bound estimate assuming plane strain deformation

The process of spin-forging of a cone involves unsteady-state deformation. However, as a first approximation for the determination of the tangential component force it will be assumed that the effect of radius change during the deformation can be neglected. This assumption is supported by the experiments conducted by the author which show that no appreciable change is observed in the magnitude of the spin-forging forces as the diameter of the formed cone increased during the operation. The spin-forging process has some features similar to that of tube drawing or ironing and the process may also be assumed to occur under the conditions of plane strain deformation if the wall thickness of the formed cone is small compared with the radius of the cone.

An expression for an upper-bound estimate of the forming parameters to cause plastic deformation is obtained by considering any kinematically admissible velocity field which satisfies both the velocity boundary conditions and the condition of incompressibility of the material. The upper-bound theorem states that the calculated force by considering any admissible displacement field can be found for which the rate of internal energy dissipation exceeds the rate of external work done. This theorem can be deduced from the principle of maximum work given by Hill (16) that the mean rate of plastic working is

$$dW/dt \leq k \left[\int_V \dot{\gamma} dv + \int_S V^* ds \right] \quad (14)$$

where, k , is the yield shear stress of the material, $\dot{\gamma}$, is the maximum shear strain rate in an element of volume, dv , and, V^* , is the magnitude of the tangential velocity discontinuity

occurring on a line of length, s.

Johnson (17) has shown that the upper bound approach to problems in metal forming is useful for determining average forming pressure in plane strain processes and for indicating the probable mode of deformation. Kudo (18) applied his concept of a "unit deforming region", which was originally for plane strain, to the solution of axisymmetric problems, since the rigorous solution of three dimensional problems is very complex and no exact solution is presently available. Kobayashi (19, 20) developed the kinematically admissible velocity field proposed by Kudo by replacing the conical surface with curved surfaces of velocity discontinuity. However, this does not imply that improved upper bound estimates are thereby obtained in all cases.

A tentative upper bound estimate for the dimensionless tangential component force, $F_t / \bar{\sigma} t_o f$, during the spin-forging of cones has been obtained assuming plane strain deformation and considering a kinematically admissible velocity field comprising a single triangle of tangential velocity discontinuities on a median plane given by

$$F_t / \bar{\sigma} t_o f = \{R/2 \sin \beta \sin \phi \sin(\beta + \phi)\} \left[\{\sin^2 \phi / (1-R)\} + \sin^2 \beta - \sin 2\phi \sin(\beta + \phi) \right. \\ \left. + (1-R) \sin^2(\beta + \phi) + \{R/(1-R)\} \sin^2 \phi \sin \beta \right] \quad (15)$$

where the optimum value of the angle, ϕ , which gives a least upper bound estimate was determined for various values of fractional reduction in wall thickness, R , by use of a computer. (see appendix 2 and for computer programme see appendix 5).

The main assumptions made in deriving the above expression for the dimensionless tangential component force are that :

- (a) steady state flow prevails,
- (b) the material is homogeneous and isotropic,
- (c) no elastic deformation occurs and consequently there are no volumetric changes,
- (d) the material is non-strain-hardening,
- (e) the Tresca yield criterion is applicable,
- (f) thermal effects can be neglected,
- (g) the workpiece-mandrel interface is perfectly smooth,
- (h) the workpiece-roller interface is perfectly rough,
- (i) the tool would have a flat working surface.

It should be appreciated that other admissible patterns of tangential velocity discontinuity could be considered depending on the value of the fractional reduction in wall thickness, R . The proposed velocity field comprising a single triangle of tangential velocity discontinuities is usually regarded as being appropriate for moderate fractional reductions whilst for a low fractional reduction a "vertical type" of

multi-triangular field is likely to be more appropriate. For a high fractional reduction, it is known that an improved upper bound solution may be obtained by assuming a velocity field comprising two or more triangles in line.

However, in this study no attempt has been made to effect this kind of refinement. In any case, this would certainly lead to a greater complexity of the solution that may not be justified.

3.4 Upper-bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a median plane.

A more systematic approach for obtaining a good upper-bound estimate was proposed by Kudo (18, 21, 22). He introduced the concept of the "unit rectangular deforming region" and also by considering several types of admissible velocity field in the deforming regions, he suggested that a better upper-bound solution may be obtained by considering "a rigid triangle velocity field". According to Kudo this is because the triangle velocity field always gives a lower rate of energy dissipation than the parallel velocity field and the calculation of the energy is relatively simple. This type of velocity field has also been proposed by Green (23) for plane strain drawing and ironing which may be applied to an axisymmetric flow through conical dies because the deformation pattern is very similar. Therefore Green's

assumption in plane strain drawing that, $\theta = \phi + \beta$ is valid in this axisymmetric upper-bound approach. See figure A.3 (2).

The assumed velocity field comprises straight lines of tangential velocity discontinuity on a median plane and the dimensionless tangential component force, $F_t/\bar{\sigma}$ to f , for this velocity field is then given by

$$F_t/\bar{\sigma} \text{ to } f = \{R/2\} \left[\{\sin\phi \sin\beta / (1-R) \sin^3(\beta+\phi)\} + \{\sin\beta / \sin\phi \sin(\beta+\phi)\} + \{R \sin\phi / (1-R) \sin\beta \sin(\beta+\phi)\} + \{(1-R)(1+\ln R)\} \right] \quad (16)$$

The assumptions made in section 3.3 for the plane strain upper-bound solution are applicable here and the optimum value of the angle, ϕ , to give a least upper-bound estimate was determined by the same method as before (see appendix 3).

3.5 Upper-bound estimate assuming axisymmetric deformation and curved surfaces of tangential velocity discontinuity on a median plane.

The treatment of the admissible velocity field for axisymmetric deformation suggested by Kudo is restricted to that of conical surfaces as surfaces of velocity discontinuity in the deforming regions. This is because of the assumption that the velocity discontinuities on the median plane occur along straight lines. Kobayashi (19, 20) proposed admissible velocity fields which may contain

curved surfaces of velocity discontinuity instead of conical surfaces. This type of velocity field was found to give a better upper-bound for some problems of inward flow, outward flow and flow through conical dies.

The development of upper-bound solutions for the flow through conical dies which is typical of many forming processes, such as the extrusion or drawing of bars may be achieved by considering a similar velocity field to that proposed by Green for plane strain deformation and improving the velocity field by replacing the conical surfaces with curved surfaces.

An expression for an upper-bound estimate of the forming forces to cause plastic deformation is obtained by calculating the energy of deformation of a continuously deforming body and the energies dissipated due to shearing along the surfaces of velocity discontinuity which are based on an assumed velocity field. The approximate upper-bound solution for axisymmetric deformation is a useful method since there is no exact mathematical solution for three-dimensional problems presently known.

The upper-bound estimate for the dimensionless tangential component force, $F_t / \bar{\sigma} t_0 f$, during the spin-forging of cones assuming steady state axisymmetric deformation involving curved surfaces of velocity discontinuity is

given by

$$\begin{aligned}
 F_t / \bar{\sigma} \cdot t_o \cdot f = \{ R^2 / 4(1-R) \} & \left[-2 / \sin(\beta + \phi) - \{ 2(1-R)^2 \sin 2(\beta + \phi) / \sin \phi \sin \beta \} \right. \\
 & + \{ 2 \sin(\beta + \phi) \cos \phi / \sin \beta \sin \phi \} + 2 / \sin \phi - \{ (1-2R) \sin \phi \sin \beta \\
 & \ln (1-R) / R \sin^2(\beta + \phi) \} - \{ (1-2R) \cos(\beta + \phi) \ln(1-R) / R \} - \{ (1-2R) \\
 & \sin \beta \ln(1-R) / R \sin(\beta + \phi) \} - \{ (1-2R) \cos \phi \ln(1-R) / R \} - \{ (1-2R) \sin \phi \\
 & \ln (1-R) / \sin \beta \sin(\beta + \phi) \} + \{ 4 \sin \phi \sin \beta (4 + (1-2R)^2)^{1/2} / \sin(\beta + \phi) 3^{1/2} \} \left. \right]
 \end{aligned}$$

The assumptions made in obtaining the previous upper-bound solution are applied in this solution (see appendix 4).

CHAPTER FOUR

DESCRIPTION OF THE
EXPERIMENTAL EQUIPMENT

DESCRIPTION OF THE EXPERIMENTAL EQUIPMENT

4.1 Centre lathe with hydraulic copying attachment

The spin-forging operations were performed using a Lodge and Shipley "Powerturn 1600" robust centre lathe capable of continuously variable spindle speed between 13 - 2000 rev / min. An integral revolution counter enabled approximate adjustment of the spindle speed and the feed range available was from 0.19 mm / rev to 1.17 mm / rev. The machine was equipped with a hydraulic copying attachment and this was essential for a spin-forging or spinning operation since the stylus follows the contour of the template and enables the machine to copy the motions a craftsman would use to produce a particular shape. Photographs showing the general arrangement of the experimental spin-forging equipment are presented in figures 4 and 5.

4.2 Rollers used for spin-forging

A spin-forging roller was designed and fabricated from an annealed carbon chrome alloy steel, oil hardened and tempered to Rockwell C 62 and ground to an external diameter of 10 cm, having a "round off" radius

of 4 mm. There are many shapes of roller used. Some typical roller shapes commonly used for spin-forging operations are illustrated in figure 6. The common feature of all these rollers are the "round off" portion of radius, the "land" and "front", as shown in figure 6 (a).

From this variety of possibilities, a common roller was selected with a "land" machined on one side to ensure that the feed marks were removed when the higher feed rates were employed during the operations. It may be helpful to add a "front" to the roller as shown in figure 6 (a) to push the flange forward and also prevent rubbing of the flange on the side of the roller when the condition of true spin-forging does not prevail. Combined needle-roller bearings were fitted into the roller housing capable of withstanding a radial force of 4.5 KN and an end thrust of 3.24 KN. The roller assembly which is shown in figure 7 (a) was mounted in a forked holder with its surface ground to ensure a good contact with the measuring platform of the three component force dynamometer which is referred to later.

Another roller with the same shape as the first one with "round off" radius of 5 mm, was made and fitted

with tapered roller bearings capable of withstanding a radial force of 8.7 KN and an end thrust of 6 KN for the second part of the experiments where 70 /30 brass material was used.

4.3 Mandrel adapter and replaceable conical mandrel heads

A series of conical mandrel heads with semi-cone angles of 30, 35 , 40 and 45 degrees were machined from mild steel to a ground finish, since the external surface of the mandrel produces the same standard of surface finish inside the formed conical workpiece. A mandrel adapter was machined from mild steel as shown in figure 7(b) having a threaded axial hole to enable the selected conical head to be secured to the adapter which, in turn, was clamped into a four jaw chuck on the lathe.

4.4 The three component force dynamometer

The three component force dynamometer used in the experimental work consisted of a measuring platform and was of the piezo-electric transducer type, suitable for measuring component forces in three mutually orthogonal directions. For each of the three component forces a proportional electric charge is generated in the measuring platform.

These charges are fed into amplifiers where they are converted into proportional output voltages which could be recorded by means of a U.V. recorder. The maximum force range of the dynamometer for the tangential direction (X) is 5KN and for the radial (Y) and axial (Z) directions are both 10KN. The measuring platform was designed on the basis of quartz three-component force measuring elements, which will give a very rigid measuring system with correspondingly high resonant frequency, so that very rapid force changes can be measured.

4.5 Templates

For each mandrel head having a particular cone semi-angle, α , a corresponding template having the same angle, α , was machined from mild steel to a ground finish to be used in conjunction with the hydraulic copying attachment on the lathe. This arrangement ensured that the roller traversed parallel to the mandrel head and the cone produced would have a uniform wall thickness. A dummy rotating centre was also used to hold the blank against the mandrel end by means of pressure exerted at the tailstock of the lathe.

4.6 Specimen materials

Circular specimens of 12 cm diameter and 2 mm thickness were cut on a Edwards circular cutter from commercially pure aluminium sheet which could be clamped to the face of the rotating conical mandrel. The material used for the first part of the experiment was commercially pure aluminium (SIC grade) in the half-hard condition purchased in sheet of 200 cm x 100 cm and 2 mm thickness. Commercially pure aluminium was selected because it is one of the commonly used materials for spinning and spin-forging applications and also the limitation on the capacity of the dynamometer and the roller bearings. The maximum force which could be safely carried by the roller bearings were, 4.5KN in the radial and 3.24KN in the axial direction. For the second part of the experiment when 70 / 30 brass was used another roller was designed and manufactured with higher load carrying bearings capable of withstanding a radial force of 8.7KN and an end thrust of 6KN.

4.7 Calibration equipment

The calibrations of the rollers were performed using an Avery, type 7110 DCJ 250KN universal testing machine operated as a force indicating, slow hydraulic press.

A dummy tool was made from mild steel having a profile identical with that of the actual roller. A heavy, ground box angle plate was also used for mounting the dummy tool and the measuring platform on the table of the testing machine when the pressure was applied in the radial and axial directions. A cylindrical punch was made from hardened and tempered alloy steel with a 5mm diameter shank to be used for exerting the force on the dummy tool.

4.8 Lubrication

Lubricants have been used in the past during spinning operations to reduce the power consumption and to dissipate some of the heat which was generated. The need for using a lubricant during spin-forging operations is a matter of opinion since in most of the recent work published in this field (9, 12, 13) it is shown that the use of a lubricant does not affect the forming forces and the power consumption significantly. The kind of lubricant which has been used in the past for spinning operations varies with the type of material being spun. The lubricants established for most drawing or rolling applications have also been used for spinning and there has not been any definable rules for the choice of

lubricant. This has been largely a matter of personal preference based on practical experience. Some of the all-purpose lubricants used for various types of metal are oil-based lubricant, soap solution, mixture of talc and paraffin oil and bees wax.

In this particular study, no lubricant was used so that this variable could be eliminated from consideration particularly as it appears that friction does not have appreciable influence on the spin-forging forces and this was supported by the experiments conducted by the author which showed that the magnitudes of the spin-forging forces remained sensibly unchanged during both the dry and lubricated conditions. This is possibly due to the fact that the relative velocity between the cone and the roller is very low. Also, in spin-forging, the workpiece normally has a surface-to-volume ratio which is lower than in many metal working operations ; consequently, the friction work will be small compared with the energy dissipated to cause the deformation of the workpiece.

CHAPTER FIVE

EXPERIMENTAL

PROCEDURE

EXPERIMENTAL PROCEDURE

5.1 Preliminary tests

A number of spinning operations were initially performed using an ordinary spinning tool in order to become familiar with the technique of spinning and gain some practical experience as well as defining the problem areas which exist and their consequences on the actual metal spinning. These initial spinning tests were followed by some preliminary tests to establish the range of mandrel speed and feed rate for which successful spin-forging operations could be effected. An approximate range of mandrel speed and feed rate were required to be known, firstly to give a high efficiency on the speed of operation and, secondly, because it was desired to determine some forming forces which could be measured within the limitations of the dynamometer and roller load carrying capacity. These preliminary tests indicated that a mandrel speed of between 600 - 800 rev / min with a moderate feed rate would be suitable for most of the softer metals, such as commercially pure aluminium, copper and 70/30 brass.

5.2 Calibration of the three component force dynamometer

The dynamometer and roller tool were arranged to measure the three components of the resultant force

exerted at the workpiece-roller interface during the spin-forging operations. However, since the point of contact between the workpiece and roller would not be on the centre line of the measuring platform, the suitability of the dynamometer for the purpose had to be initially assessed and the effect of this misalignment determined by means of a static calibration for the particular tool geometry employed.

The calibration was performed in an assembled state using a dummy tool having a profile identical with that of the actual roller assembly used in the spin-forging operations. A schematic view of the arrangement is given in figure 8. This dummy tool was bolted to the measuring platform of the dynamometer which, in turn, was then clamped onto the table of a universal testing machine. A cylindrical punch of 5 mm diameter was used to exert the force on the dummy tool. The force was initially exerted on the dummy tool to correspond with the tangential component (x direction) of the resultant force. This force was varied by increments of 0.5KN up to a maximum of 5KN and then relieved. During the loading and unloading phases the outputs from the dynamometer were monitored by means of a U.V. recorder which produced record traces of the tangential component force (x direction) and the corresponding interactions in the radial (y direction) and the axial (z direction).

This procedure was repeated to obtain U.V. records for the radial and axial component forces respectively and their corresponding interactions in the other two directions.

Four calibration graphs were produced, given in figures 9 to 12, showing the relationship between the U.V. galvanometer deflection and the actual applied force in the tangential (X), radial (Y), axial (Z) and centre directions respectively, together with the interactions recorded in the other two corresponding directions for each case. In each case, the relationship was found to be linear over the range examined and the maximum interactions occurred when the force exerted was in the axial (Z) direction, such that in the radial (Y) direction the interaction was -2.0 per cent of maximum deflection and +2.4 per cent in the tangential (X) direction. These interactions could be attributed mainly to the fact that the point of application of the applied force in the axial (Z) direction was 13 cm from the front edge of the dynamometer platform and 1 cm displaced from its centre line.

The results of the calibration indicated that the proposed arrangement for the dynamometer and roller assembly was suitable for the determination of the three components of the resultant force exerted at the workpiece-roller interface during the spin-forging operations.

5.3 Determination of the tangential, radial and axial component forces at varying mandrel speed and constant feed rate

Circular specimens of 12 cm diameter and 2 mm thickness were cut by a circle cutting machine from commercially pure aluminium sheet of deep drawing quality. After cleaning, the centre of each specimen was marked to assist in positioning on the end face of the mandrel. The mandrel adapter was mounted in a four jaw chuck ensuring that it was coaxial with the spindle axis and the mandrel head of 35 degree semi-cone angle was then tightly screwed to the adapter. A circular aluminium specimen was then clamped centrally onto the end face of the mandrel conical head by means of pressure exerted by the dummy rotating centre inserted in the tailstock of the lathe.

The contact surfaces of the roller holder, measuring platform and the cross-slide were initially cleaned. Then the roller assembly was bolted to the measuring platform of the dynamometer which, in turn, was mounted on the cross-slide of the lathe. To ensure that the roller would travel parallel to the side of the mandrel conical head, the cross-slide was rotated through an angle of 35 degrees anticlockwise from the normal position. In addition, the cross-slide was adjusted accurately until the gap between the point of contact of the roller and the mandrel

conical head was the desired final thickness of the formed conical workpiece. This final thickness was determined for the particular mandrel semi-cone angle from the sine-law equation (1) to ensure true spin-forging. Deviation from this thickness setting results in the flange being distorted during the operation. After securing the template, the hydraulic copying attachment was engaged and, by using a sensitive dial gauge, the travel of the cross-slide parallel to the side of the mandrel conical head was checked to ensure that the formed conical workpiece would have a uniform wall thickness.

A spindle speed of 100 rev / min and a saddle feed rate of 4.1 cm / min were initially selected and the cross-slide was moved to the starting position where the roller just made contact with the circular blank. The spin-forging operation commenced by engaging the saddle feed when the desired mandrel speed was attained and was terminated when the whole flange was deformed to produce a conical workpiece. During the operation, the three component forces were recorded simultaneously using the U.V. recorder with a paper speed of 10 mm / s. In addition, the mandrel speed of 100 rev / min and a saddle feed rate of 4.1 cm / min were noted.

The U.V. traces were measured to obtain the magnitudes of the tangential, radial and axial components of the resultant force exerted at the workpiece-roller interface. These traces indicated that the magnitudes of the tangential and radial component forces decreased slightly with increase in the diameter of the formed cone during a spin-forging operation. Mean values of the magnitudes of these component forces were therefore obtained from the U.V. traces.

This procedure was then repeated for the constant feed rate of 4.1 cm / min but with mandrel speeds of 200, 300, 400, 500, 600, 800 and 1000 rev / min. The whole procedure was again repeated for the same values of mandrel speed but with saddle feed rates of 6.4 and 10.2 cm / min. In a spin-forging operation, the feed rate dictates the speed of metal displacement where the feed rate is considered as the axial speed of the saddle and in this particular study is specified as the axial travel of the saddle per minute.

Factors which were considered during each experiment included, ensuring that the spin-forging specimen remained a tight fit on the mandrel conical head, the flange remained at 90 degrees to the axis of rotation, that a built-up edge in front of the roller

was absent and any deviation from the sine-law value of the wall thickness was noted. The same procedure was employed for the 70 / 30 brass specimens.

5.4 Determination of the tangential, radial and axial component forces at varying feed rate and constant mandrel speed

Employing a similar procedure to that discussed in the previous section, the tangential, radial and axial component forces were determined for a constant mandrel speed of 600 rev / min. However, the feed rate was varied from 1 to 40 cm / min. in increments of 5 cm / min. The same procedure was adopted for the 70 / 30 brass specimens.

5.5 Determination of the tangential, radial and axial component forces using mandrels having various cone semi-angles

By adopting a similar procedure to that discussed previously, the tangential, radial and axial component forces were determined for mandrels having various cone semi-angles. However, a mandrel head having a cone semi-angle of 30 degrees was initially used with constant mandrel speed of 600 rev / min and saddle feed rate of 6.4 cm / min. The three component forces were recorded simultaneously as previously, and the whole procedure was then repeated using the

mandrel conical heads having cone semi-angles of 35, 40 and 45 degrees. However, the mandrel speed and saddle feed rate were maintained constant throughout the experiments at 600 rev / min and 6.4 cm / min respectively. The same procedure was adopted for the 70 / 30 brass specimens.

5.6 Determination of the tangential, radial and axial component forces at varying fractional reduction in wall thickness of formed cones having a constant cone semi-angle of 35°

The effects of fractional reduction in wall thickness on the three component forces during a spin-forging operation were investigated by adopting a similar procedure to that discussed previously maintaining a constant mandrel speed of 600 rev / min and a feed rate of 4.1 cm / min. The three component forces were recorded as the fractional reduction in wall thickness of the formed cones was varied from 0 to 0.65 generally in increments of 0.1. The same procedure was employed for the 70 / 30 brass specimens.

5.7 Determination of the tangential component force using mandrels having various cone semi-angles at various mandrel speed

Employing a similar procedure to that described in

the previous section, the tangential component force was determined for a constant feed rate of 8.3 cm / min using the mandrel having a cone semi-angle of 30° and at mandrel speeds of 200, 300, 400, 500, 600, 800 and 1000 rev / min. This entire procedure was again repeated for the same values of mandrel speed and feed rate but using the other mandrels having cone semi-angles of 35, 40 and 45 degrees. The same procedure was used for the 70 / 30 brass specimens.

5.8 Determination of the tangential component force using mandrels having various cone semi-angles at varying feed rate

By adopting a similar procedure to that discussed in section 5.7, the tangential component force was determined for a constant mandrel speed of 600 rev / min. However, the feed rate was varied from 1 to 40 cm / min in increments of 5 cm / min. The entire procedure was repeated using the other mandrels having cone semi-angles of 35, 40 and 45 degrees respectively. The same procedure was employed with the 70 / 30 brass specimens.

5.9 Determination of the quasi-static true compressive stress-compressive natural strain curves for commercially pure aluminium and 70/30 brass at ambient temperature

Previous authors (7, 9, 12) who have investigated the mechanics of the spin-forging of cones have utilised the results from uniaxial tensile tests to evaluate the average flow stress, $\bar{\sigma}$, for purposes of determining the magnitude of the tangential component force, F_t , usually for the range of natural strain up to the condition of instability in uniaxial tension.

However, the stress state in the plastically deforming region under the roller is predominantly compressive. Also, the strains involved, particularly at the higher values of fractional reduction in wall thickness, exceed the strain to instability in uniaxial tension by a significant factor. It is therefore considered more appropriate to refer to the compressive true stress-natural strain characteristic curve for the deforming material. There are, however, considerable difficulties in obtaining such a curve for the appropriate range of strain using specimens from the sheet material. Nevertheless, it is preferred in this study to refer

to a compressive characteristic curve to determine the appropriate average flow stress, $\bar{\sigma}$, for comparing and assessing the validity of the various elementary theories and the proposed upper-bound estimates for the dimensionless tangential component force.

The characteristic curves for commercially pure aluminium and 70 / 30 brass which are presented in figures 13 and 14 respectively, were obtained by means of slow compression tests performed on two cylindrical specimens machined from the sheet material such that the mean compressive strain-rate was less than $10^{-3}/s$.

Before commencing a test, the entire specimen and platen faces were lubricated with graphite in tallow to minimise frictional resistance at the specimen-platen interfaces and thus prevent "barrelling" of the specimen. After the specimen was compressed by 1 mm the test was interrupted and the specimen was machined to return the height to diameter ratio to its initial value of unity. The original diameter of the specimens was 2 mm when carefully cut from sheet metal with diameter to height ratio of one and the compression of the specimen was continued with only

one machining operation in between until a height of 0.5 mm was reached. Before continuing a test, the specimen and platens were cleaned with a degreasing agent, dried and re-lubricated.

CHAPTER SIX

RESULTS AND

DISCUSSION

6.1 Effect of mandrel speed on the three component forces during the spin-forging operations at constant feed rate

In these investigations the effects of variation in the mandrel speed on the three component forces during the spin-forging of cones having a constant semi-cone angle of 35 degrees with the feed rate maintained constant at 4.1 cm / min were studied. The relationships existing between the tangential, radial and axial components of the resultant force exerted at the workpiece-roller interface and the mandrel speed for the constant feed rate of 4.1 cm / min are shown in figure 15 and 16 for commercially pure aluminium and 70 / 30 brass respectively. The results obtained showing the relationship between the tangential component force and mandrel speed at constant feed rates of 4.1, 6.4 and 10.2 cm / min for commercially pure aluminium are given in figure 17.

Referring to figure 15, it will be seen that each of the three component forces decreases as the mandrel speed increases tending to become asymptotic to the mandrel speed axis. It will also be noted that the tangential component force is affected to the greater extent and the axial component force to the least extent by variation in the mandrel speed. A very similar relationship between the three component forces and the mandrel speed were obtained with 70 / 30 brass as shown in figure 16. As could be

anticipated, figures 17 and 18 show that as the feed rate was increased from 4.1 to 10.2 cm / min, the magnitude of the tangential component force increased. However, the general form of the relationship between the tangential component force and the mandrel speed remained unchanged.

During the spin-forging operations, when a constant feed rate of 4.1 cm / min was employed, an increase of mandrel speed was associated with a decrease in the magnitude of the three component forces until a mandrel speed of approximately 600 rev / min was attained. Further increase of mandrel speed is shown in figure 15 not to affect the three component forces significantly. A similar situation exists for the other two constant feed rates of 6.4 and 10.2 cm / min as can be seen by referring to figure 17. However, the corresponding mandrel speeds are approximately 700 and 800 rev / min respectively. The results, therefore, indicate that there is an *optimum* mandrel speed existing for a given cone angle and workpiece material corresponding to a constant feed rate, that is, there is a least value of mandrel speed corresponding to a minimum value of the tangential component roller force.

Although this optimum mandrel speed is likely to be dependent on the workpiece material, cone angle and the dimensions of the formed cone, the results indicate

that the optimum mandrel speed is affected most significantly by the feed rate. By comparing figures 15 and 16 it will be seen that the workpiece material has relatively less influence on the optimum mandrel speed than the cone angle and the feed rate employed. Thus, by referring to figures 17 and 18 it is possible to select an optimum mandrel speed corresponding to a given feed rate for a specific application. It must be emphasised that when interpreting the results, these optimum mandrel speeds are only valid for the condition of true spin-forging and any deviation from this condition will affect the mandrel speed and its optimum value as well as the three components of the resultant force exerted at the workpiece-roller interface.

In the past, the selection of correct mandrel speed associated with feed rate has been mainly a matter of experience. Although more experimental evidence is necessary to establish that an optimum mandrel speed exists for various materials and product geometries, it is now evident as shown by figures 17 and 18 for commercially pure aluminium and 70 / 30 brass respectively that an optimum mandrel speed is likely to exist.

6.2 Effect of feed rate on the three component forces during spin-forging operations at constant mandrel speed

In figures 19 and 20 the effect of feed rate on the tangential, radial and axial components of the resultant force exerted at the workpiece-roller interface is shown graphically for commercially pure aluminium and 70 / 30 brass respectively. These results indicate that the three component forces during the spin-forging operations vary as the feed rate. In particular, the tangential component varies linearly with the feed rate whilst the radial and axial components do not. It will also be seen from figure 19 that, as the feed rate increases, the axial component does not increase significantly and attains a maximum of 1.16 KN at a feed rate of 30 cm / min. A similar situation was observed for 70 / 30 brass as shown in figure 20. These latter results indicate that the radial component force also follows the same pattern as the axial component force in the case of the 70 / 30 brass and these two component forces do not increase significantly as the feed rate increases and reaches a value of approximately 40 cm / min.

Since the rate of metal flow is closely related to the feed rate for a given workpiece diameter then

in order to increase the production rate the feed rate should normally be increased. However, the feed rate is limited by the power available at the mandrel and the capacity of the roller bearings. Because the axial power required by the cross-slide is not greatly affected by an increase in the feed rate this variation is not of great importance. Also, since the velocity in the radial direction approaches zero, the radial power required is insignificant. Therefore, the additional energy dissipated per unit time in producing plastic deformation as the feed rate increases can be regarded as being mainly dependent on the increase in tangential component force.

It was observed, during the spin-forging of commercially pure aluminium cones, that the surface finish of the formed cones was satisfactory for feed rates not exceeding approximately 15 cm / min. At feed rates exceeding this value, feed marks were observed on the external surface of the formed cones. In addition, the formation of a built-up edge in front of the roller was initiated when the feed rate approached a value of approximately 30 cm / min and became increasingly pronounced as the feed rate increased beyond this value.

Although the feed rate is likely to be the main cause of built-up edge in front of roller, this phenomenon is also dependent on the softness of the workpiece material, cone angle (which dictates the fractional reduction in thickness) and the roller configuration. It should be noted that with the 70 / 30 brass specimens the built-up edge in front of the roller was very seldom apparent within the range of feed rate employed during these investigations.

6.3 Relation between the three component forces and the mandrel cone semi-angle at constant mandrel speed and feed rate

Figures 21 and 22 show the effect of mandrel cone semi-angle on the tangential, F_t , radial, F_r and axial F_a , components of the resultant force exerted at the workpiece-roller interface for commercially pure aluminium and 70 / 30 brass respectively. By referring to these figures, it will be observed that each of the three component forces varies inversely proportional to the mandrel cone semi-angle, α , and the tangential component force is affected to the greatest extent. This relationship could be anticipated because as the cone semi-angle, α , increases, the fractional reduction in wall thickness, $R = 1 - \sin \alpha$, decreases such that the effective strain involved is reduced and hence the tangential component force

decreases. By comparing figures 21 and 22, it will be seen that the three component forces for commercially pure aluminium are affected to a greater extent by the cone semi-angle, α , than for the 70 / 30 brass. This could be partly attributed to the yield strength of the two workpiece materials.

It should be emphasised that, during the true spin-forging of cones, the wall thickness of the formed cone is dictated by the cone semi-angle, α . Therefore, when considering a particular application, the shape of the finished workpiece is accompanied by a certain reduction in wall thickness. For example, a conical workpiece having a cone semi-angle of 30° is only associated with a fractional reduction in wall thickness, R , of 0.5 which represents a limitation of the process for true spin-forging conditions. Any deviation from this fractional reduction will result in an unsatisfactory surface finish and a complex stress state under the roller will develop so that predicting the tangential component force and the nature of the formed workpiece becomes extremely difficult. Therefore, in any particular application, if the shape and the final thickness of the cone is fixed then to produce this particular shape under true spin-forging condition the original blank thickness

has to be determined from the equation, $t_o = t_f / \sin\alpha$,
to produce a cone with a desired wall thickness.

6.4 The dependence of the three component forces
on the fractional reduction in wall thickness
during spin-forging operations with a constant
cone semi-angle of 35° and constant mandrel
speed and feed rate

Figures 23 and 24 show graphically the relationship existing between the tangential, radial and axial components of the resultant force exerted at the workpiece-roller interface and the fractional reduction in wall thickness of the formed conical workpiece for commercially pure aluminium and 70 / 30 brass respectively. By referring to figures 23 and 24 it will be observed that these component forces are dependent on the final thickness of the formed workpiece and reducing the final thickness increases the magnitudes of the three component forces. However, an important feature of the curves is that inflexions are evident in the relationships for the tangential and radial component forces occurring at a fractional reduction in wall thickness of approximately 0.43.

When the fractional reduction in wall thickness was less than the sine-law thickness, which for a cone semi-angle $\alpha = 35^\circ$, is given by

$$R = 1 - \sin\alpha = 1 - 0.5736 \approx 0.43$$

the flange tended to move forward and made contact with the roller. The rubbing action of the flange on the roller significantly affected the magnitude of the tangential and radial component forces, and set up a very complex state of stress in the rest of the flange. Under severe conditons, wrinkling of the flange occurred in the circumferential direction and the surface finish of the formed cone was unsatisfactory. Figure 25 shows the wrinkling of the flange in the circumferential direction for 70 / 30 brass which usually occurs at the beginning of the operation when the flange is at its maximum radius. This could be due to the fact that, since wrinkling is a condition of instability in compression such as balking and is a transitional phenomenon occurring at the interface between the elastic and plastic deformation states. It is therefore most likely to occur at the beginning of a spin-forging operation when the flange radius is near a maximum. Although this wrinkling phenomenon is likely to be dependent on the thickness and the workpiece material, during the operation of spin-forging it was observed that it is also affected significantly by deviation from true spin-forging condition.

It was observed during these investigations that 70 / 30 brass had a lesser tendency to wrinkle than the commercially pure aluminium and this is due to the higher yield strength of the 70 / 30 brass. The maximum diameter of the formed cone was less than the diameter of the original blank. This condition will be referred to here as *under spin-forging*.

Under condition of true spin-forging, that is, the fractional reduction in wall thickness $R = 0.43$ for a cone semi-angle of 35° , in the case of commercially pure aluminium the flange remained straight making an angle of 90° to the axis of rotation. The surface finish was satisfactory and the maximum diameter of the formed cone was equal to the diameter of the original blank. Also, when the wall thickness of the formed cone was less than that dictated by the sine-law value, the magnitude of the tangential and radial component forces initially increased at a rate greater than that during under spin-forging and the flange tended to move backwards away from the roller, this bending of the flange taking place in advance of the roller. Cracks were developed on the external surface of the commercially pure aluminium workpiece

at the root of the flange as the flange distorted and fracture occurred in some cases when the fractional reduction in wall thickness attained a value of approximately 0.65. This condition is referred to as *over spin-forging*.

6.5 Relation between the tangential component force and mandrel speed using mandrels having various cone semi-angles

The relationship existing between the tangential component force and the mandrel speed for cone semi-angles, α , of 30°, 35°, 40° and 45° at a constant feed rate of 8.3 cm / min is presented in figure 26 for commercially pure aluminium and at a constant feed rate of 6.4 cm / min for 70 / 30 brass in figure 27. Results obtained showing the relationship between the mandrel cone semi-angle, α , and the optimum mandrel speed at a constant feed rate of 8.3 cm / min deduced from figure 26 together with similar results corresponding to constant feed rates of 4.1 cm / min and 6.4 cm / min are given in figure 28 for the commercially pure aluminium workpiece.

By referring to figure 26, it will be seen that for different values of cone semi-angle, α , the relationships between the tangential component force, F_t , and mandrel speed have the same characteristics. It

may also be noted that, as the cone semi-angle increases from 30 to 45° , the optimum mandrel speed increases from approximately 700 to 850 rev / min.

Figure 28 also indicates that the optimum mandrel speed varies directly proportional to the cone semi-angle, α , for each of the feed rates considered, but it is also affected by variation in feed rate. Thus, it is important when selecting an optimum mandrel speed to consider the effect of the cone semi-angle as well as feed rate.

By referring to figure 29 it will be seen that the values of optimum mandrel speed for $70 / 30$ brass are slightly lower than those for commercially pure aluminium. Nevertheless, both materials exhibit the same characteristic results and the optimum mandrel speed increases as the cone semi-angle, α , increases, that is, decrease in the fractional reduction in thickness. Therefore, it can also be deduced from the results that as the fractional reduction in wall thickness decreases the optimum mandrel speed increases accordingly.

6.6 Relation between the tangential component force and feed rate using mandrels having various cone semi-angles

The relationship between the tangential component

force and the feed rate for the four mandrel cone semi-angles of 30, 35, 40 and 45 degrees when the mandrel speed remained constant at 600 rev / min are shown in figures 30 and 31 for commercially pure aluminium and 70 / 30 brass respectively. These results indicate that, in each case, the tangential component force, F_t , varies directly proportional to the feed rate and the relative effect becomes less as the cone semi-angle, α , increases from 30 to 45°. By referring to figures 30 and 31, it is possible to select the highest feed rate for a given cone semi-angle, α , corresponding to the power available at the mandrel in order to perform the most efficient spin-forging operation for a particular application.

6.7 Uniform plastic deformation energy method for determination of the dimensionless tangential component force during the spin-forging of cones

Figure 32 shows the values of the dimensionless tangential component force during the spin-forging of cones predicted by the uniform deformation theory and the experimental values for varying fractional reduction in wall thickness, R , of the formed workpiece.

It will be seen from figure 32 for commercially pure aluminium that the predicted values given by the uniform deformation theory do not provide a good correlation with the experimental values for the dimensionless tangential component force. The predicted values deduced from expression 13 of section 3.2 give an over-estimate of the dimensionless tangential component force for the lower values of fractional reduction in wall thickness. This could be mainly due to the assumption made during the solution, that, the total power consumed during the deformation is due to the tangential component force alone and neglecting the power consumed by the radial and axial component forces. It will be noted that for higher values of fractional reduction in wall thickness, that is for $R > 0.6$ the two curves of predicted values and the experimental results will deviate from each other and the theoretical values become lower than the experimental results. This could be mainly due to neglecting the redundant straining and nonhomogeneity of the deforming metal. It will also be seen from figure 32 that the experimental curve is convex downwards whilst the theoretical curve is convex upwards.

Equation 13 of section 3.2 is very similar to the

solution presented by Kalpakcioglu (9) for spin-forging of cones assuming a simple shear deformation. Both the solutions, for higher values of fractional reduction in wall thickness produce an underestimate of the dimensionless tangential component force, F_t , since the effective strain, $\bar{\epsilon}$, used in these solutions does not include redundant and any other straining. An improved analytical solution is therefore necessary to give a better correlation with the experimental values and also the right characteristic shape for the predicted values of dimensionless tangential component force.

6.8 Upper bound estimate for the dimensionless tangential component force during the spin-forging of cones

The values of the dimensionless tangential component force during the spin-forging of cones predicted by the elementary theories, an upper bound estimate assuming plane strain deformation and the experimental values for varying values of fractional reductions in wall thickness, R , of the formed workpiece are given in figure 33. Figure 34 shows the values of dimensionless tangential component force during the spin-forging of cones predicted by an upper

bound estimate assuming

- (a) plane strain deformation
- (b) axisymmetric deformation with plane surfaces of velocity discontinuity on a median plane
- (c) axisymmetric deformation with curved surfaces of velocity discontinuity
- (d) the experimental values

for varying fractional reduction in wall thickness of the formed commercially pure aluminium workpiece.

By referring to figure 33 it will be seen that for the lower values of fractional reduction in wall thickness, the correlation between the predicted values given by the simple shear deformation theory and the experimental values for the dimensionless tangential component force is relatively close. However, for higher fractional reduction in wall thickness, the simple shear theory deviates from the experimental curve and a better correlation is achieved with the upper-bound estimate assuming plane strain deformation. It is significant that the two curves for the upper bound estimate and the experimental result have a similar characteristic shape, that is, they are both concave upwards and the correlation improves for the higher values of fractional

reduction in wall thickness. For values of fractional reduction less than, say, 0.4 it could be expected that an improved upper-bound estimate for the dimensionless tangential component force would be obtained by assuming a "vertical type" of multi-triangular velocity field instead of a single triangular field.

As would be expected, assuming homogeneous deformation, predicts dimensionless tangential component force values which are a considerable underestimate. For a fractional reduction in wall thickness of 0.5 the value is approximately 25 per cent below that predicted by the simple shear theory which, in turn, is about 10 per cent below the actual experimental value.

By referring to figure 34, it will be seen that for lower values of fractional reduction in wall thickness of less than approximately 0.4 both upper-bound estimates assuming axisymmetric deformation give values of dimensionless tangential component force which are considerable underestimates. However, for higher values of fractional reduction the predicted values by the two axisymmetric assumed upper-bounds are overestimates and the best correlation is achieved with the upper-bound estimate assuming plane strain deformation. This could be mainly due to the fact that the

deformation in spin-forging operation is very close to plane strain deformation since the diameter is large relative to the wall thickness of the cone. Better results may be obtained by assuming axisymmetric deformation for problems of bar-extruding and wire drawing.

Despite the various assumptions made in deriving the expression giving an upper bound estimate assuming plane strain deformation, an acceptable correlation with the experimental results is achieved. It will be noted that for the higher values of fractional reduction in wall thickness in excess of $R = 0.4$ the upper bound estimate provides an overestimate.

However, it should be borne in mind that the discrepancy between the upper bound estimate and the experimental values could be partly attributed to the assumed value of the average flow stress, $\bar{\sigma} = 85 \text{ MPa}$ for commercially pure aluminium of the deforming material. This value was derived from the compressive true stress-natural strain characteristic curve of figure 13 and was used to determine the experimental values of the dimensionless tangential component force.

Figure 35 shows the comparison between the experimental values of the dimensionless tangential component force for 70 / 30 brass with assumed value of the average flow stress, $\bar{\sigma} = 250$ MPa derived from the compressive true stress-natural strain characteristic curve of figure 14 and those corresponding values predicted by the various upper-bound estimates. The correlation between predicted values and the experimental results is very similar to that of figure 34 for commercially pure aluminium.

CHAPTER SEVEN

CONCLUSIONS AND
RECOMMENDATIONS FOR
FUTURE WORK

CONCLUSIONS

7.1 Conclusions

From the results obtained by experimentation and analytical solution the following conclusions may be drawn :

- (1) During the spin-forging of cones, the tangential, radial and axial component forces vary inversely as the mandrel speed and an *optimum* mandrel speed exists for a given cone angle and workpiece material corresponding to a constant feed rate.
- (2) At constant mandrel speed, the tangential component force varies linearly with the feed rate, whilst the other two component forces do not. A built-up edge may be formed in front of the roller at feed rates exceeding 30 cm / min.
- (3) During the spin-forging of cones the tangential, radial and axial component forces vary inversely proportional to the mandrel cone semi-angle when the mandrel speed and feed rate are maintained constant.
- (4) It has been shown that the magnitudes of the three component forces increase with increase in the fractional reduction in wall thickness of the formed cone. However, inflexions occur in the relationships which exist between the tangential

and radial component forces and the fractional reduction in wall thickness. When using a mandrel having a semi-cone angle $\alpha = 35^\circ$, these inflexions occur at a fractional reduction in wall thickness of about 0.43 which corresponds with that for the condition of true spin-forging. Spin-forging operations may thus be defined in terms of three regimes : Under spin-forging when the fractional reduction in wall thickness $R < 1 - \sin \alpha$
 True spin-forging when $R = 1 - \sin \alpha$ and over spin-forging when $R > 1 - \sin \alpha$

- (5) Although the wrinkling of the flange is dependent on the workpiece material and its thickness, it is also most significantly affected by the deviation from true spin-forging conditions.
- (6) At constant feed rate, the *optimum* mandrel speed varies directly proportional to the mandrel cone semi-angle and increases with feed rate.
- (7) At constant mandrel speed, the tangential component force varies directly proportional to the feed rate for various values of mandrel cone semi-angle but the effect is reduced as the cone semi-angle increases.
- (8) No appreciable changes were observed in the magnitudes of the spin-forging forces during both dry and lubricated condition.

- (9) The dimensionless tangential component force calculated from uniform plastic deformation solution does not provide a good correlation with the experimental results and the predicted values are an under-estimate for higher fractional reduction in wall thickness.
- (10) An upper-bound estimate assuming plane strain deformation for the dimensionless tangential component force considering a kinematically admissible velocity field comprising a single triangular field appears to provide the best correlation with the experimental values.

7.2 Recommendations for future work

- (1) The present work has been limited to the two work-piece materials, commercially pure aluminium and 70 / 30 brass. Further work should be carried out on other materials including steels to determine their optimum speed and feed rate to perform a particular spin-forging application in the most efficient way.
- (2) Work should be continued using a number of different shapes and size of roller to determine the relationship between the forming forces and the roller configuration. The results obtained using the two rollers of different "round off" radius during the present

study indicate that as the "round off" radius increased, the tangential component force increased slightly.

- (3) In the analytical solution presented in this study, it was assumed that the change in radius of the cone during the spin-forging operation did not effect the forming forces significantly. This assumption was supported by the experimental results which showed that there are no appreciable changes in the magnitudes of the spin-forging forces as the radius of the forming cone changed. A more accurate solution might be obtained by treating the problem to be an incremental deformation.
- (4) In the previous work done in this field to date the nature of deformation has been considered to be of a shear type deformation. A more intensive study should be conducted to determine the pattern of the deformation in more detail and also to evaluate the effect of the process variables discussed in this study on the actual deformation pattern.
- (5) The "after effect" of spin-forging is of a great interest. A detailed investigation should be carried out to study this "after effect" by determining the residual stresses, the change in physical and metallurgical properties during the

spin-forging operation and also how these properties are affected by altering the process variables discussed in this study.

REFERENCES

REFERENCES

1. H. KUDO and M. YOKAI Investigations into the helical rolling processes, Proc. 8th Intern. Mach. Tool Des. and Res. Conf., University of Manchester (1967)

2. T. Z. BLAZYNSKI and I. M. COLE An analysis of redundant deformation in rotary piercing, Proc. Instn. Mech. Engrs., pt. 1, No. 33, P. 867 (1963-4)

3. W. JOHNSON and G. NEEDHAM Experiment in ring rolling, Intern. J. Mech. Sci., 10, P 95 (1968)

4. R. A. C. SLATER, N. K. BARCOAH, E. APPLETON and W. JOHNSON The rotary forging concept and initial work with an experimental machine, Proc. Instn. Mech. Engrs. 184, pt. 1, No. 32, P.577 (1969-70)

5. E. APPLETON and R. A. C. SLATER Effect of upper platen configuration in the rotary forging process and rotary forging into a contoured lower platen, Intern. J. Mach. Tool Des. and Res., 13, P. 43 (1972)

6. O. KIENZLE Entwicklungs linier der werkzeugmaschinen der Umformtechnik, Werkstattstechnik, 49, P.479 (1959)

7. R. A. PAULTON and B. N. COLDING Two new industrial processes for plastic deformation of metal, Proc. Conf. Technol. Eng. Manuf., Instn. Mech. Engrs. (London), P.54 (1958)

8. P. W. BRIDGMAN "Studies in large plastic flow and fracture", McGraw-Hill, New York, chapter one (1952)

9. S. KALPAKCIOGLU "An application of theory to an engineering problem; power spinning", 9th Sagamare Ordnance Materials Research Conference (1962)

10. S. KALPAKCIOGLU On the mechanics of shear spinning, Trans. Amer. Soc. Mech. Engrs., Ser. B, J. Engng. for Ind. 83, P. 125 (1961)

11. S. KALPAKCIOGLU A study of shear spinnability of metals, Trans. Amer. Soc. Mech. Engrs. Ser. B, J. Engng. for Ind. 83, P 478 (1961)

12. B. AVITZUR and
C. T. YANG Analysis of power spinning of cones, Trans. Amer. Soc. Mech. Engrs., Ser. B, J. Engng. for Ind., 82, P. 231 (1960)

13. S. KOBAYASHI,
I. K. HALL and
E. G. THOMSEN A theory of shear spinning of cones, Trans. Amer. Soc. Mech. Engrs., J. Engng for Ind., P 485, (1961)

14. R. L. KEGG "A new test method for determination of spinnability of metals" Trans. Amer. Soc. Mech. Engrs. Series B, J. Engng. for Ind., 83 P.119 (1961)

15. A. YOUNGER and
A. J. FOSTER An investigation into power assisted metal spinning, 17th Intern. Mach. Tool Des. and Rec. Conf. (1976)

16. R. HILL "Mathematical theory of plasticity" Clarendon Press, Oxford, Chapter 6 (1950)

17. W. JOHNSON Estimation of upper bound loads for extrusion and coining operations, Proc. Instn. Mech. Engrs., 173, P. 61 (1959)

18. H. KUDO Some analytical and experimental studies of axisymmetric cold forging and extrusion - I, Intern. J. Mech. Sci., 2, P.102 (1959)

19. S. KOBAYASHI Upper-bound estimate for axisymmetric forming problem - 1, Trans. Amer. Soc. Mech. Engrs., J. Engng. for Ind. P. 122 (1964)
20. S. KOBAYASHI Upper-bound solution of axisymmetric forming problems - II, Trans. Amer. Soc. Mech. Engrs., J. Engng. for Ind., P.326 (1964)
21. H. KUDO An upper-bound approach to plane strain forging and extrusion - 1, Intern. J. Mech. Sci., 2, P. 57 (1960)
22. H. KUDO An upper-bound approach to plane strain forging and extrusion - II Intern. J. Mech. Sci, 2, P 229 (1960)
23. A. P. GREEN Calculation on theory of sheet drawing, B.I.S.R.A. Report No. MW/B/7/52 (1952)

BIBLIOGRAPHY

1. S. KALPAKCIOGLU
Maximum reduction in power spinning of tubes, Trans. Amer. soc. mech. Engrs., ser B,J. Engng. for Ind., P.49 (1964).
2. S. KOBAYASHI and E. G. THOMSEN
Theory of spin-forging, C.I. R.P. Annalen, 10, No. 2, P. 114 (1964).
3. H. C. SORTAIS, S. KOBAYASHI and E. G. THOMSEN
"Mechanics of conventional spinning", Trans. Amer. Soc. Mech. Engrs., ser. B, J. Engng for Ind. 85, P. 346 (1963)
4. R. G. LONGLEY and W. HADLEY
Metal flow turning, Sheet Metal Industries, Vol. 33, No. 352, P. 519 (1956).
5. T. R. MOHAN and R. MISHRA
Studies on power spinning of tubes. Intern. J. of prod. Res., P.351, Oct. (1972).
6. F. J. PISTOL
Modern spinning method, Sheet Metal Industries, P.131, Feb. (1970).
7. W. F. WAIKER
Metal spinning, Tooling, Oct. P.15 (1969).
8. C. H. WELLS
The control of build up and diametral growth in shear forming, Trans. Amer. Soc. Mech. Engrs., ser. B, J. Engng for Ind. P.162 (1968)

APPENDIX
ONE

APPENDIX I

Uniform plastic deformation energy

The process of spin-forging of a cone from sheet metal may be assumed to be a uniform deformation and the work of plastic deformation is determined from the shape of an element before and after the process.

To examine the velocity and strain rate field under the roller, the cone is considered in the $r \ \theta \ z$ cylindrical co-ordinate system (see figure A.1), where, r , is in the radial direction, θ , in the circumferential and, z , in the axial direction which is the axis of symmetry. The velocities in these directions are given by

$U_r = 0$ (changes in the radial direction are very small and are assumed to be zero).

$U_\theta = 2\pi rN$ (rotational motion of the cone).

$$U_z = \frac{dz}{dt} = \phi \left[\left(\frac{dz}{d\theta} \right), \left(\frac{dz}{dN} \right), \left(\frac{dz}{dr} \right) \right] \quad \left. \vphantom{\frac{dz}{dt}} \right\} \text{ (A.1.(1))}$$

(this is the velocity in z direction which is some function of axial, radial and circumferential velocities and ϕ denotes function of ...)

The components of the velocity vector obtained above are used to compute the components of the strain rate tensor. The condition for cylindrical symmetry gives;

$$\begin{aligned} \dot{\epsilon}_r &= \frac{\partial U_r}{\partial r}, \quad \dot{\epsilon}_\theta = \frac{U_r}{r} + \frac{1}{r} \frac{\partial U_\theta}{\partial \theta}, \quad \dot{\epsilon}_z = \frac{\partial U_z}{\partial z} \\ \dot{\gamma}_{rz} &= \frac{\partial U_r}{\partial z} + \frac{\partial U_z}{\partial r} \quad \& \quad \dot{\gamma}_{r\theta} = \dot{\gamma}_{z\theta} = 0 \end{aligned} \quad \text{A.1.(2)}$$

Now substituting the components of the velocity vector from equation A.1.(1) into equation A.1.(2) gives,

$$\dot{\epsilon}_r = \frac{\partial U_r}{\partial r} = 0 \quad (\text{since } U_r = 0).$$

$$\dot{\epsilon}_z = \frac{\partial U_z}{\partial z} = 0 \quad (\text{since the velocity in the axial direction is constant}).$$

$$\dot{\epsilon}_\theta = \frac{U_r}{r} + \frac{1}{r} \frac{\partial U_\theta}{\partial \theta} = 0 \quad (\text{since } \dot{\epsilon}_r + \dot{\epsilon}_\theta + \dot{\epsilon}_z = 0 \text{ for incompressibility}).$$

$$\dot{\gamma}_{rz} = \frac{\partial U_r}{\partial z} + \frac{\partial U_z}{\partial r} = 0 + \frac{\partial U_z}{\partial r} = \frac{\partial U_z}{\partial r}$$

now the strain-rate field becomes,

$$\dot{\epsilon}_r = \dot{\epsilon}_\theta = \dot{\epsilon}_z = \dot{\gamma}_{r\theta} = \dot{\gamma}_{z\theta} = 0 \quad \& \quad \dot{\gamma}_{rz} = \frac{\partial U_z}{\partial r} \quad \text{A.1.(3)}$$

The effective strain rate assuming the von Mises yield criterion to be applicable is

$$\frac{\dot{\epsilon}}{\epsilon} = 2/\sqrt{3} \sqrt{\frac{1}{2} \dot{\epsilon}_{ij} \dot{\epsilon}_{ij}} \quad \text{or}$$

$$\frac{\dot{\epsilon}}{\epsilon} = \frac{2}{\sqrt{3}} \left[\frac{1}{2} (\dot{\epsilon}_r^2 + \dot{\epsilon}_\theta^2 + \dot{\epsilon}_z^2) + \frac{1}{4} (\dot{\gamma}_{rz}^2 + \dot{\gamma}_{r\theta}^2 + \dot{\gamma}_{z\theta}^2) \right]^{\frac{1}{2}} \quad \text{A.1.(4)}$$

substituting the values of the strain rate components from equation

A.1.(3) into equation A.1.(4) produces,

$$\frac{\dot{\epsilon}}{\epsilon} = (2/\sqrt{3}) \left[\frac{1}{4} \frac{\partial U_z^2}{\partial r^2} \right]^{\frac{1}{2}} = (1/\sqrt{3}) \frac{\partial U_z}{\partial r} \quad \text{A.1.(5)}$$

State of stress;

For cylindrical symmetry the conditions for the components of the stress tensor are $\tau_{z\theta} = \tau_{r\theta} = 0$ and the other four independent components of the stress tensor may not vanish. The static

equilibrium equation thus reduces to

$$\frac{\partial \sigma_r}{\partial r} + \frac{\partial \tau_{rz}}{\partial z} + \frac{\sigma_r - \sigma_\theta}{r} = 0$$

$$\frac{\partial \tau_{rz}}{\partial r} + \frac{\partial \sigma_z}{\partial z} + \frac{\tau_{rz}}{r} = 0$$

and the Levy-Mises stress strain-rate relationships become,

$$\frac{\dot{\epsilon}_r}{\sigma_r - \sigma_m} = \frac{\dot{\epsilon}_\theta}{\sigma_\theta - \sigma_m} = \frac{\dot{\epsilon}_z}{\sigma_z - \sigma_m} = \frac{\dot{\gamma}_{rz}}{2\tau_{rz}}$$

where the hydrostatic stress $\sigma_m = \frac{1}{3} (\sigma_r + \sigma_\theta + \sigma_z)$

and the von Mises' yield condition is given by

$$\frac{1}{2} \left[(\sigma_r - \sigma_\theta)^2 + (\sigma_\theta - \sigma_z)^2 + (\sigma_z - \sigma_r)^2 \right] + 3\tau_{rz}^2 = \bar{\sigma}^2$$

In this particular axisymmetric deformation where $\dot{\epsilon}_r = \dot{\epsilon}_\theta$, as shown in equation A.1.(3), the corresponding stress deviator and stress components are equal. Therefore, $\sigma_r = \sigma_\theta$, so the static equilibrium equations reduce to

$$\left. \begin{aligned} \frac{\partial \sigma_r}{\partial r} + \frac{\partial \tau_{rz}}{\partial z} &= 0 \\ \frac{\partial \tau_{rz}}{\partial r} + \frac{\partial \sigma_z}{\partial z} + \frac{\tau_{rz}}{r} &= 0 \end{aligned} \right\} \quad \text{A.1.(6)}$$

and the yield condition becomes,

$$(\sigma_r - \sigma_z)^2 + 3 \tau_{rz}^2 = \frac{2}{\sigma} = k^2$$

but from von Mises yield criterion the value of, k, which can be evaluated experimentally from the uniaxial tensile test

$$\text{is, } k = \frac{\sigma_0}{\sqrt{3}}$$

where, σ_0 , is the flow stress.

Now the rate of energy dissipation per unit Volume for a von Mises material is

$$\dot{W} = (2/\sqrt{3}) \sigma_0 \sqrt{\frac{1}{2} \epsilon_{ij} \epsilon_{ij}} \quad \text{A.1.(7)}$$

Substituting for $\dot{\epsilon}$ from equation A.1.(5) into equation A.1.(7) gives,

$$\dot{W} = (1/\sqrt{3}) \sigma_0 \frac{\partial U_z}{\partial r} \quad \text{A.1.(8)}$$

The rate of energy dissipation for the deformed cone becomes,

$$\dot{W} = \int_{\text{volume}} (1/\sqrt{3}) \sigma_0 \frac{\partial U_z}{\partial r} dV \quad \text{A.1.(9)}$$

where dV is an infinitesimal volume of r dθ dr dz

$$\text{but } \frac{\partial U_z}{\partial r} = \frac{\partial}{\partial r} U_z \text{ and } U_z = \frac{\partial z}{\partial T}$$

From equation A.1.(1)

$$dz = \frac{\partial z}{\partial r} dr + \frac{\partial z}{\partial \theta} d\theta + \frac{\partial z}{\partial n} dn \quad \text{A.1.(10)}$$

$$\text{and } U_z = \frac{dz}{dT} = \frac{\partial z}{\partial r} \frac{dr}{dT} + \frac{\partial z}{\partial \theta} \frac{d\theta}{dT} + \frac{\partial z}{\partial n} \frac{dn}{dT} \quad \text{A.1.(11)}$$

where n = number of revolution passed from time T = T₀ to the instant considered.

$$n = N (T - T_0) \quad \frac{dn}{dT} = N$$

$$\frac{d\theta}{dT} = \frac{U_{\theta}}{r} = 2\pi N$$

$$\frac{dr}{dT} = U_r = 0$$

Substituting the above derivatives into equation A.1.(11) gives,

$$U_z = \frac{\partial z}{\partial \theta} 2\pi N + \frac{\partial z}{\partial n} N = N \left[2\pi \frac{\partial z}{\partial \theta} + \frac{\partial z}{\partial n} \right] \quad \text{A.1.(12)}$$

It is possible to deduce the above terms from the geometry of the roller and approximate equations for a torus. However, this requires a great deal of work and yields a solution which would be very complex.

Some approximation will therefore be made which are not expected to effect the results significantly but lead to simplicity. Neglecting the term $\frac{\partial z}{\partial n}$ from equation A.1.(12) which could be very small produces,

$$U_z = 2\pi N \frac{\partial z}{\partial \theta} \quad \text{A.1.(13)}$$

Equation A.1.(9) can now be written as:

$$\dot{W} = (\sigma_o/\sqrt{3}) \int_{\text{surface}} \left[\int_0^{t_o} \frac{\partial U_z}{\partial r} dz \right] dS \quad \text{A.1.(14)}$$

where dS is an infinitesimal area of $r d\theta dr$.

Since the strains are independent of the Z direction. After integrating with respect to Z we have,

$$\dot{W} = (\sigma_o/\sqrt{3}) t_o \int_{\text{surface}} \frac{\partial U_z}{\partial r} dS \quad \text{A.1.(15)}$$

or

$$\dot{W} = (\sigma_o/\sqrt{3}) t_o \int_{\theta} \int_r \frac{\partial U_z}{\partial r} r dr d\theta \quad \text{A.1.(16)}$$

By assuming that r can be regarded as constant and integrating

$$\int_r r \frac{\partial U_z}{\partial r} dr \text{ we have}$$

$$\dot{W} = (\sigma_o/\sqrt{3}) t_o r \int_{\theta} U_z d\theta \quad \text{A.1.(17)}$$

Substituting for U_z from equation A.1.(13) into A.1.(17) gives,

$$\dot{W} = (\sigma_0/\sqrt{3}) t_0 r \int_{\theta} 2\pi N \frac{\partial z}{\partial \theta} \cdot d\theta \quad \text{A.1.(18)}$$

Taking $Z = f \cos \alpha$, where, f , is the axial feed of the roller and, α , is the cone semi-angle and substituting it into equation A.1.(18) and integrating gives,

$$\dot{W} = (2\pi/\sqrt{3}) \sigma_0 t_0 r N f \cos \alpha \quad \text{A.1.(19)}$$

Assuming that the power required to cause the deformation during spin-forging of cones is due to the tangential component force alone,

$$\text{Hence } \dot{W} = \text{total power} = F_t V_t$$

$$\text{or } F_t = \dot{W}/V_t \quad \text{A.1.(20)}$$

Substituting for, $\dot{W}$, from equation A.1.(19) into equation A.1.(20) gives,

$$F_t = (2\pi/\sqrt{3}) \sigma_0 t_0 r N f \cos \alpha / 2\pi r N$$

or

$$F_t = (1/\sqrt{3}) \sigma_0 t_0 f \cos \alpha \quad \text{A.1.(21)}$$

Finally

$$F_t/\sigma_0 t_0 f = (1/\sqrt{3}) \cos \alpha \quad \text{A.1.(22)}$$

However, after equation A.1.(22), it is now shown that uniform deformation energy method produces the same solution as that assuming simple shear given by Kalpakcioglu [10].

APPENDIX
TWO

APPENDIX 2

Upper-bound estimate for the dimensionless tangential component force during the spin-forging of a cone assuming plane strain deformations. (Johnson Upper-Bound).

To simplify the roller configuration, it was assumed that the roller would have a flat surface. Also a velocity is impressed on the whole system so that the roller can be considered to only rotate about its axis with no axial advance and the element of the blank is moving with an axial velocity equal to unity towards the roller. Considering the above simplifications the situation may therefore be considered analogous to flow through a conical die.

A pattern of tangential velocity discontinuities comprising a single triangular field as shown in the physical diagram of figure A.2(2) is proposed. The corresponding hodograph is that given in figure A.2(b).

From the physical diagram of figure A.2(a);

$$OB = (t_o - t_f) / \sin \beta \quad A.2(1)$$

$$BC = t_f / \sin \phi \quad A.2(2)$$

From hodograph of figure A.2(b),

$$\overline{OC} / \sin(\beta + \phi) = \overline{bC} / \sin \beta$$

Therefore

$$\overline{bc} = \overline{OC} \sin\beta / \sin(\beta + \phi) = t_o \sin\beta / t_f \sin(\beta + \phi) \quad A.2(3)$$

similarly

$$\overline{ob} = \overline{bC} \sin\phi / \sin\beta = t_o \sin\phi / t_f \sin(\beta + \phi) \quad A.2(4)$$

Also

$$\left. \begin{aligned} \overline{bx} &= \overline{ob} \sin\beta \\ \overline{ox} &= \overline{ob} \cos\beta \\ \overline{ax} &= \overline{ox} - 1 = \overline{ob} \cos\beta - 1 \end{aligned} \right\} \quad A.2(5)$$

From the hodograph of figure A.2(b),

$$(\overline{ab})^2 = (\overline{bx})^2 + (\overline{ax})^2 \quad A.2(6)$$

substituting for $\overline{bx}$ and $\overline{ax}$ from equation A.2(5) into equation A.2(6) and simplifying produces

$$(\overline{ab})^2 = \frac{\left\{ t_o^2 \sin^2\phi - 2t_o t_f \sin(\beta + \phi) \sin\phi \cos\phi + t_f^2 \sin^2(\beta + \phi) \right\}}{t_f^2 \sin^2(\beta + \phi)} \quad A.2(7)$$

Since the triangle, DKG, in the physical diagram and the triangle, b x a, in the hodograph of figures A.2(b) are similar

$$AB/DK = \overline{ab}/\overline{bx}$$

Therefore

$$AB = (\overline{ab}/\overline{bx})DK = (\overline{ab}/\overline{ob}\sin\beta)t_o \quad A.2(8)$$

Assuming the workpiece-roller interface to be perfectly rough,

then the rate of internal energy dissipation is given by,

$$dE/dt = k \sum S V^* = k \left[(AB \cdot \overline{ab}) + (BC \cdot \overline{bc}) + (OB \cdot \overline{ob}) \right] \quad A.2(9)$$

It can then be shown that the rate of external work done is

$$\begin{aligned} dW/dt &= F_a \cdot 1 \leq dE/dt = \\ &= k \left[(t_o/t_f \sin \beta) \left\{ 1/(\sin \beta \cot \phi + \cos \beta) \right\} + t_o/(\cos \phi \sin \phi + \cot \beta \sin^2 \right. \\ &\quad \left. \phi) - (2t_o \cos \phi / \sin \beta) + t_f (\cot \phi + \cot \beta) + R \sin \phi / t_f \sin(\beta + \phi) \right] \end{aligned} \quad A.2(10)$$

Combining equation (9) from section 3.1 and the equation A.2(10) and assuming t_o is unity so that, $t_f = 1 - R$, then it can be deduced that

$$\begin{aligned} F_t / \bar{\sigma} t_o f &= \left\{ R / 2 \sin \beta \sin \phi \sin(\beta + \phi) \right\} \left[\left\{ \sin^2 \phi / (1 - R) \right\} + \sin^2 \beta - \sin 2\phi \sin(\beta + \phi) \right. \\ &\quad \left. + (1 - R) \sin^2(\beta + \phi) + \left\{ R / (1 - R) \sin^2 \phi \sin \beta \right\} \right] \end{aligned} \quad A.2(11)$$

where, $\bar{\sigma}$, is the average flow stress, f , is the feed per revolution and, R , is the fractional reduction in wall thickness.

The least upper-bound estimate for the dimensionless tangential component force given by equation A.2(11) is obtained by determining the optimum value of the angle ϕ . Differentiating equation A.2(10) with respect to, ϕ , substituting $t_o = 1$, $t_f = 1 - R$ and simplifying gives

$$\dot{dW}/d\phi = k \left[\left\{ \sin\beta \sin^2\phi - (1-R) \sin^2\beta \sin(\beta+2\phi) + 2(1-R) \sin^3\phi \sin^2(\beta+\phi) - (1-R)^2 \sin\beta \sin^2(\beta+\phi) + R \sin\beta \sin^2\phi \right\} / \sin\beta \sin^2\phi \sin^2(\beta+\phi) \right] \quad A.2(12)$$

If $\dot{W} = dW/dt$ is to have a minimum value, then $\dot{dW}/d\phi = 0$

Hence

$$\sin\beta \sin^2\phi - (1-R) \sin^2\beta \sin(\beta+2\phi) + 2(1-R) \sin^3\phi \sin^2(\beta+\phi) - (1-R)^2 \sin\beta \sin^2(\beta+\phi) + R \sin\beta \sin^2\phi = 0 \quad A.2(13)$$

The optimum value of the angle, ϕ , corresponding to various values of fractional reduction in wall thickness, R , may then be determined. In the present study these values were obtained with the aid of a computer (see appendix 5 for computer programme).

For the particular roller used in the spin-forging operations, the angle of inclination of the land, β , was $\pi/4$.

APPENDIX
THREE

APPENDIX 3

Upper-bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a median plane.
(Kudo upper-bound).

The type of velocity field proposed by Green [23] for plane strain drawing may also be considered valid for an axisymmetric flow through a conical die. Therefore, the same pattern of tangential velocity discontinuities for the previously assumed plane strain deformation in appendix 2 is used with the following assumptions made:

- (a) unity initial thickness,
- (b) unity width,
- (c) unity axial speed.

The assumed velocity field shown in figure A.3(2) comprises straight lines of tangential velocity discontinuity on a median plane and four "unit deforming regions", O, A, B and, C, are thus considered with a radial (u) and axial (v) component of velocity in these regions. Region "O" assumed to be stationary with region "A" moving as a rigid body with an axial velocity equal to unity. Region "C" is also assumed to move as a rigid body and its axial velocity assuming no change in the width and because of volume constancy,

$$v_a/v_c = t_f/t_o$$

substituting for v_a and t_o being equal to unity,

Therefore, $v_c = 1/t_f$

A.3(1)

Also the equations for the surfaces of velocity discontinuity with unity width and unity initial thickness are:

$$\left. \begin{aligned} z_{AB} &= 1/\sin\theta \\ z_{BC} &= t_f/\sin\phi \\ z_{OB} &= R/\sin\beta \end{aligned} \right\} \begin{array}{l} \text{(length of velocity discontinuity} \\ \text{surface x unity width).} \end{array} \quad \text{A.3(2)}$$

The total rate of internal energy dissipation assuming the Tresca yield criterion to be applicable is given by

$$dE/dt = k \left[\int_V \dot{\gamma} \, dv + \int_S v^* \, ds \right] \quad \text{A.3(3)}$$

or

$$dE/dt = \dot{E}_1 + \dot{E}_2 \quad \text{A.3(4)}$$

where, $\dot{E}_1$, is the rate of energy dissipation of a continuously deforming region and, $\dot{E}_2$, is the rate of internal energy

dissipation occurring on the surfaces of velocity discontinuity.

$$\dot{E}_1 = \int_0^L \int_{t_f}^{t_0} \dot{\epsilon} \, 2\pi r \bar{\sigma} \, dt \, dL \quad \text{A.3(5)}$$

or

$$\dot{E}_1 = 2\sigma \pi Lr \int_{t_f}^{t_0} \dot{\epsilon} \, dt \quad \text{A.3(6)}$$

where, $\bar{\sigma}$, is average flow stress = $2k$, L , is the axial length of the cone and, $\dot{\bar{\epsilon}}$, is the effective strain rate given by

$$\dot{\bar{\epsilon}} = \left[(2/3) \left\{ \dot{\epsilon}_r^2 + \dot{\epsilon}_\theta^2 + \dot{\epsilon}_z^2 + (1/2) \dot{\gamma}_{rz}^2 \right\} \right]^{1/2} \quad \text{A.3(7)}$$

where, $\dot{\epsilon}_r$, $\dot{\epsilon}_\theta$, $\dot{\epsilon}_z$ and $\dot{\gamma}_{rz}$ are components of the strain rate tensor and may be determined from the velocity field in region "B" of the physical diagram shown in figure A.3(a). For the sake of simplicity and avoiding a complex solution for determining the effective strain rate, $\dot{\bar{\epsilon}}$, a homogeneous deformation is assumed and the assumption is supported by the fact that for small fractional reduction, R , the internal power of deformation becomes very close to the ideal power of deformation (see figure 33).

Hence

$$\bar{\epsilon} = \ln(t_o/t_f) \quad \text{A.3(8)}$$

and since

$$\dot{\bar{\epsilon}} = d\bar{\epsilon}/dt = \bar{\epsilon}(\text{axial velocity of cone/length of cone}) \quad \text{A.3(9)}$$

substituting for axial velocity being unity in equation A.3(9) produces,

$$\dot{\bar{\epsilon}} = 1/L \left\{ \ln(t_o/t_f) \right\} \quad \text{A.3(10)}$$

Now substituting for $\dot{\bar{\epsilon}}$, from equation A.3(10) into equation A.3(6) produces,

$$\dot{E}_1 = 2\bar{\sigma}\pi Lr(1/L) \int_{t_f}^{t_0} \ln(t_0/t_f) dt \quad A.3(11)$$

putting, $t_0 = 1$, and integrating,

$$\dot{E}_1 = 2\bar{\sigma} \pi r \left[t_f \left\{ 1 - \ln(1/R) \right\} \right] \quad A.3(12)$$

The rate of energy dissipation occurring on the surfaces of velocity discontinuity is,

$$\dot{E}_2 = k \left[(Z_{AB} \cdot \overline{ab}) + (Z_{BC} \cdot \overline{bc}) + \psi(Z_{OB} \cdot \overline{ob}) \right] \quad A.3(13)$$

where, Z_{AB} , Z_{BC} and Z_{OB} are the areas of the surfaces of tangential velocity discontinuity given by equation A.3(2) and, $\overline{ab}$, $\overline{bc}$ and $\overline{ob}$, are the tangential velocity discontinuities on these surfaces obtained from the hodograph of figure A.2(b) and, ψ , is the fraction for frictional resistance and it becomes equal to unity for a perfectly rough interface and equal to zero for a perfectly smooth interface.

Therefore

$$\dot{E}_2 = 2\pi rk \left[(1/\sin\theta) \left\{ \sin\phi \sin\beta / t_f \sin^2(\beta+\phi) \right\} + (t_f/\sin\phi) \left\{ \sin\beta / t_f \sin(\beta+\phi) \right\} + (R/t_f \sin\beta) \left\{ \sin\phi / \sin(\beta+\phi) \right\} \right]$$

or

$$\dot{E}_2 = 2\pi rk \left[\left\{ \sin\phi \sin\beta / t_f \sin^3(\beta+\phi) \right\} + \left\{ \sin\beta / \sin\phi \sin(\beta+\phi) \right\} + \left\{ R \sin\phi / t_f \sin\beta \sin(\beta+\phi) \right\} \right] \quad A.3(14)$$

Now adding equations A.3(12) and A.3(14) together and dividing them by $2\pi r$ and equating it to the rate of external work done by the axial component force, F_a , produces,

$$F_a / \sigma t_o f = (1/2) \left[\left\{ \sin\phi \sin\beta / t_f \sin^3(\beta+\phi) \right\} + \left\{ \sin\beta / \sin\phi \sin(\beta+\phi) \right\} + \left\{ R \sin\phi / t_f \sin\beta \sin(\beta+\phi) \right\} + 2 \left\{ t_f (1 - \ln(1/R)) \right\} \right] \quad A.3(15)$$

Expressing, F_a , in terms of, F_t , by using the ratio of the projected contact areas shown in equation (9) of section 3.1 and also substituting for $t_f = 1 - R$ then,

$$F_t / \sigma t_o f = (R/2) \left[\left\{ \sin\phi \sin\beta / (1-R) \sin^3(\beta+\phi) \right\} + \left\{ \sin\beta / \sin\phi \sin(\beta+\phi) \right\} + \left\{ R \sin\phi / (1-R) \sin\beta \sin(\beta+\phi) \right\} + 2 \left\{ (1-R) (1 + \ln R) \right\} \right] \quad A.3(16)$$

APPENDIX
FOUR

APPENDIX 4

Upper-bound estimate assuming axisymmetric deformation and curved surfaces of tangential velocity discontinuity. (Kobayashi upper-bound)

The mode of deformation assumed is that shown in figure A.4.(a) comprising four "unit deforming regions". In regions 1 and 3 the velocity is uniform and has an axial component only. In region 2 the direction of the velocity vector is parallel to the surface of die and the radial component of velocity in this region is not very significant since the thickness of the cone, t_f , is small compared to the mean radius of the cone, r . In region 4, the roller is assumed to be stationary, hence there is no velocity component in this region.

In region 1, the velocity is, V_1 , and will be assumed as unity. Region 3, has an axial velocity component, V_3 , and considering the volume constancy requirements,

$$\pi(r_o - r_f)^2 V_1 = \pi(r_i - r_f)^2 V_3 \quad \text{A.4. (1)}$$

However

$$(r_o - r_f) = t_o / \cos \alpha \quad \text{A.4. (2)}$$

and

$$(r_i - r_f) = t_f / \cos \alpha \quad \text{A.4. (3)}$$

Substituting equations A.4.(2) and A.4.(3) into equation A.4. (1) produces

$$\frac{V_1}{V_3} = \frac{t_f^2}{t_o^2} \quad \text{A.4.(4)}$$

Assuming unity initial thickness, gives

$$V_3 = 1/t_f^2 \quad \text{A.4.(5)}$$

The proposed kinematically admissible velocity field for spin-forging of cones is shown in figure A.4.(b). In region 2, for the normal component of velocity to be continuous across the surface AB, gives,

$$V_1 \sin \theta = V_2 \sin (\theta - \beta) \quad \text{A.4.(6)}$$

putting $V_1 = 1$ produces,

$$V_2 = \frac{\sin \theta}{\sin(\theta - \beta)} \quad \text{A.4.(7)}$$

Similarly, the normal components of velocity across the surface BC must be continuous therefore V_2 can be expressed in terms of ϕ and β as follows

$$V_2 = \frac{\sin \phi}{t_f^2 \sin(\beta + \phi)} \quad \text{A.4.(8)}$$

The velocity discontinuities occurring at the surface AB, BC and AC illustrated in figure A.4.(b) are

$$V_{AB}^* = V_1 \cos \theta + V_2 \cos (\theta - \beta) \quad \text{A.4.(9)}$$

substituting for $V_1 = 1$ and for V_2 from equation A.4.(7) into equation A.4.(9) produces,

$$V_{AB}^* = \cos \theta + \sin \theta \cot (\theta - \beta) \quad \text{A.4.(10)}$$

Also

$$V_{BC}^* = V_2 \cos (\phi + \beta) - V_3 \cos \phi \quad \text{A.4.(11)}$$

substituting for V_2 and V_3 from equations A.4.(8) and A.4.(5) into equation A.4.(11) produces,

$$V_{BC}^* = \frac{1}{t_f^2} \{ \sin \phi \cot (\phi + \beta) - \cos \phi \} \quad \text{A.4.(12)}$$

Also the velocity discontinuity at the surface AC, is,

$$V_{AC}^* = \frac{\sin \theta}{\sin (\theta - \beta)} \quad \text{A.4.(13)}$$

An element of the surfaces along, AB, BC and AC respectively are given as follows,

$$ds_{AB} = 2\pi r \frac{dr}{\sin\theta} \quad \text{A.4. (14)}$$

$$ds_{BC} = 2\pi r \frac{dr}{\sin\phi} \quad \text{A.4. (15)}$$

$$ds_{AC} = 2\pi r \frac{dr}{\sin\beta} \quad \text{A.4. (16)}$$

The total rate of internal energy dissipation assuming the Tresca yield criterion to be applicable is given by,

$$dE/dt = k \left[\int_V \dot{\gamma} dV + \int_S V^* ds \right] \quad \text{A.4. (17)}$$

$$\text{or } \dot{E} = \dot{E}_1 + \dot{E}_2 \quad \text{A.4. (18)}$$

where, $\dot{E}_1$, is the rate of energy dissipation of a continuously deforming region and, $\dot{E}_2$, is the rate of internal energy dissipation occurring on the surfaces of velocity discontinuity.

$$\dot{E}_1 = \int_0^L \int_{t_f}^{t_o} 2\pi r \bar{\sigma} \dot{\epsilon} dL dt \quad \text{A.4. (19)}$$

where, $\bar{\sigma}$, is average flow stress = $2k$, L , is the axial length of the cone and, $\dot{\epsilon}$, is the effective strain rate given by,

$$\dot{\bar{\epsilon}} = \left[(2/3) \left\{ \dot{\bar{\epsilon}}_r^2 + \dot{\bar{\epsilon}}_\theta^2 + \dot{\bar{\epsilon}}_2^2 + (1/2) \dot{\gamma}_{rz}^2 \right\} \right]^{1/2} \quad \text{A.4. (20)}$$

Substituting for the value of, $\dot{\bar{E}}_1$, from equation A.3.(12) (Appendix 3).

$$\dot{\bar{E}}_1 = 2\pi r \bar{\sigma} [t_f(1 - \ln(1/R))] \quad \text{A.4. (21)}$$

The rate of energy dissipation, $\dot{\bar{E}}_2$, occurring on the surfaces of velocity discontinuity, AB, BC and AC are given by,

$$\dot{\bar{E}}_2 = k \left[\int_{S_{AB}} V^* ds + \int_{S_{BC}} V^* ds + \psi \int_{S_{AC}} V^* ds \right] \quad \text{A.4. (22)}$$

substituting for various V^* and ds from equations A.4.(10) to A.4.(16) into equation A.4.(22), gives,

$$\begin{aligned} \dot{\bar{E}}_2 = \frac{\bar{\sigma}}{2} \left[\int_{r_f}^{r_o} \left\{ \cos\theta + \sin\theta \cot(\theta-\beta) \right\} \left\{ 2\pi r \frac{dr}{\sin\theta} \right\} + \int_{r_f}^{r_i} \left\{ \frac{1}{t_f^2} \right. \right. \\ \left. \left. \cot(\phi+\beta) \sin\phi - \cos\phi \right\} \left\{ 2\pi r \frac{dr}{\sin\phi} \right\} + \psi \int_{r_i}^{r_o} \left\{ \frac{\sin\theta}{\sin(\theta-\beta)} \right\} \right. \\ \left. \left\{ 2\pi r \frac{dr}{\sin\beta} \right\} \right] \quad \text{A.4. (23)} \end{aligned}$$

where, ψ , is the fraction for frictional resistance and it becomes equal to unity for a perfectly rough interface and equal to zero for a perfectly smooth interface.

$$\dot{E}_2 = \pi r \bar{\sigma} \left[\int_{r_f}^{r_o} \left\{ \cot \theta + \cot(\theta - \beta) \right\} dr + \int_{r_f}^{r_i} \left\{ \frac{1}{t_f^2} \cot(\phi + \beta) - \cot \phi \right\} dr \right. \\ \left. + \int_{r_i}^{r_o} \left\{ \frac{\sin \theta}{\sin(\theta - \beta) \sin \beta} \right\} dr \right] \quad \text{A.4. (24)}$$

Integrating equation A.4. (24) and putting $R = \frac{t_o - t_f}{t_o}$, yields,

$$\dot{E}_2 = \pi r \bar{\sigma} \left[\frac{t_o}{\cos \alpha} \left\{ \cot \theta + \cot(\theta - \beta) \right\} + \frac{t_f}{\cos \alpha} \left\{ \frac{\cot(\phi + \beta)}{t_f^2} - \cot \phi \right\} \right. \\ \left. + \frac{R \sin \theta}{\sin(\theta - \beta) \sin \beta \cos \alpha} \right] \quad \text{A.4. (25)}$$

Dividing equations A.4. (21) and A.4. (25) by $2\pi r$, adding them together and equating the sum of these equations to the rate of external work done by the axial component force, F_a , and substituting for $t_f = 1 - R$ produces,

$$F_a / \bar{\sigma} t_o f = \frac{1}{2} \left[\frac{\cot \theta + \cot(\theta - \beta)}{\cos \alpha} + \frac{\cot(\phi + \beta)}{(1-R) \cos \alpha} - \frac{(1-R) \cot \phi}{\cos \alpha} + \right. \\ \left. \frac{R \sin \theta}{\sin(\theta - \beta) \sin \beta \cos \alpha} + 2(1-R) \left\{ 1 - \ln(1/R) \right\} \right] \quad \text{A.4. (26)}$$

The axial component force, F_a , may be expressed in terms of F_t , the tangential component force by using equation (9) of section 3.1 and applying Green's assumption as in appendix 3, that $\theta = \beta + \phi$.

Therefore,

$$F_t / \bar{\sigma} t_o f = \frac{R}{2\cos\alpha} \left[\cot(\beta+\phi) + \cot \phi + \frac{\cot(\beta+\phi)}{(1-R)} - (1-R)\cot \phi \right. \\ \left. + R\cot\phi + R\cot\beta + (2-2R)\left\{1-\ln(1/R)\right\} \right] \quad \text{A.4. (27)}$$

Finally

$$F_t / \bar{\sigma} t_o f = \frac{R}{2\cos\alpha} \left[\cot(\beta+\phi) \left(\frac{2-R}{1-R} \right) + 2R \cot \phi + R \cot\beta + \right. \\ \left. (2-2R) \left\{ 1 - \ln \left(\frac{1}{R} \right) \right\} \right] \quad \text{A.4. (28)}$$

APPENDIX 5

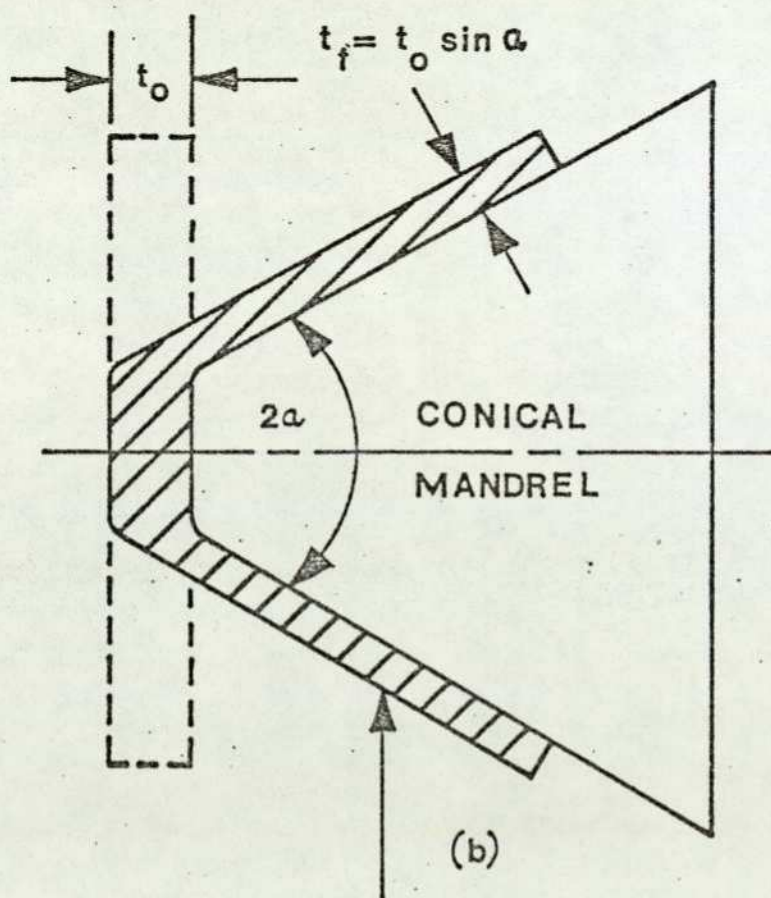
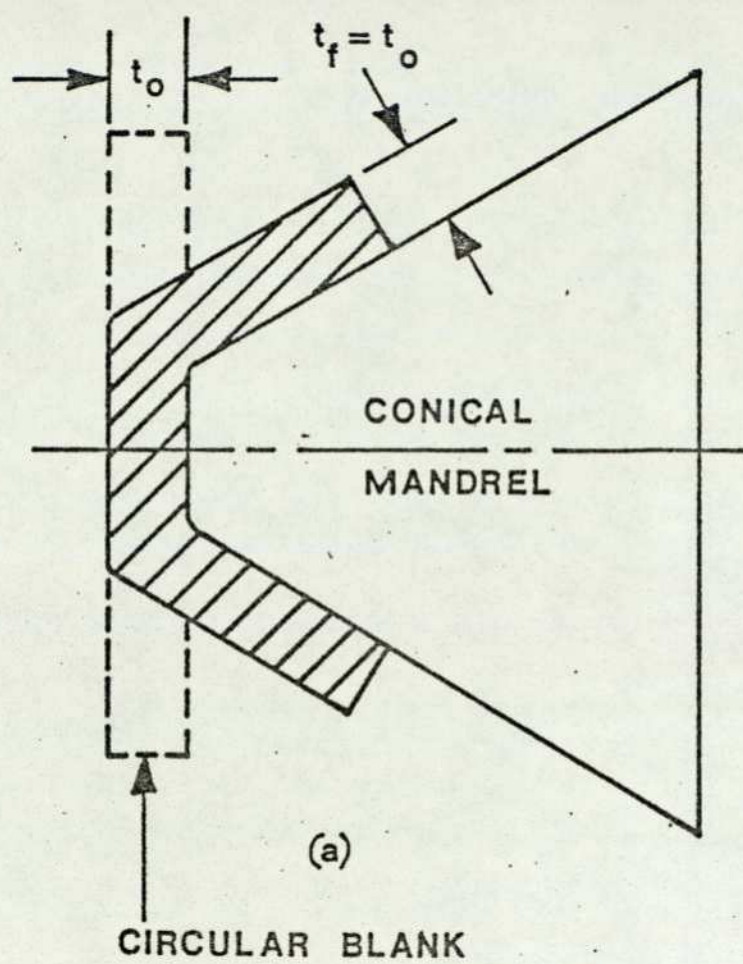
APPENDIX 5

5.1 Computer programe for evaluation of the optimum value of the angle, ϕ

```
1.  MASTER FUN
2.  DIMENSION F (12)
3.  H = 0.5
4.  DH = 0.1
5.  DALP = 0.0873
6.  DPHI = 0.1745
7.  WRITE (60,4)
8.  4 FORMAT (1X,2HFU,12X,3HPHI,11X,1HH,13X,3HALP,13X)
9.  1 H = H + DH
10.  ALP = -0.0873
11.  2 ALP = ALP + DALP
12.  N = 1
13.  PHI = -0.1745
14.  3 PHI = PHI + DPHI
15.  N = N + 1
16.  FU = SIN (ALP)*COS(PHI)*SIN(PHI)**2*SIN(ALP+PHI)**3-3*SIN(ALP)*SIN(
1PHI)**3*COS(ALP+PHI)**2+H*SIN(ALP)*SIN(ALP+2*PHI)**5
17.  F(N) = FU
18.  WRITE (60,5) FU, PHI, H, ALP
19.  5 FORMAT (1X,F13.5,1X,F13.5,1X,F13.5,1X,F13.5)
20.  IF (N.LE.11) GO TO 3
21.  IF (ALP.LE.1.5708) GO TO 2
22.  IF (H.LE.1.0) GO TO 1
23.  CONTINUE
24.  STOP
25.  END
```


5.2 Sample of the results

$\frac{dW}{d\phi}$	ϕ	t_f	α
0.00002	0.00000	0.60000	0.17460
0.00106	0.17450	0.60000	0.17460
0.01424	0.34900	0.60000	0.17460
0.04813	0.52350	0.60000	0.17460
0.07177	0.69800	0.60000	0.17460
0.06039	0.87250	0.60000	0.17460
0.04197	1.04700	0.60000	0.17460
0.04034	1.22150	0.60000	0.17460
0.02931	1.39600	0.60000	0.17460
-0.01564	1.57050	0.60000	0.17460
-0.08563	1.74500	0.60000	0.17460
0.00018	0.00000	0.60000	0.26190
0.00689	0.17450	0.60000	0.26190
0.04182	0.34900	0.60000	0.26190
0.10190	0.52350	0.60000	0.26190
0.12962	0.69800	0.60000	0.26190
0.10538	0.87250	0.60000	0.26190
0.08120	1.04700	0.60000	0.26190
0.07453	1.22150	0.60000	0.26190
0.03756	1.39600	0.60000	0.26190
-0.05207	1.57050	0.60000	0.26190
-0.17442	1.74500	0.60000	0.26190
0.00096	0.00000	0.60000	0.34920
0.01978	0.17450	0.60000	0.34920
0.08591	0.34900	0.60000	0.34920
0.17046	0.52350	0.60000	0.34920
0.19239	0.69800	0.60000	0.34920
0.15317	0.87250	0.60000	0.34920
0.12501	1.04700	0.60000	0.34920
0.10435	1.22150	0.60000	0.34920
0.02560	1.39600	0.60000	0.34920
-0.12083	1.57050	0.60000	0.34920
-0.30480	1.74500	0.60000	0.34920
0.00343	0.00000	0.60000	0.43650
0.04278	0.17450	0.60000	0.43650
0.14620	0.34900	0.60000	0.43650
0.24705	0.52350	0.60000	0.43650
0.25279	0.69800	0.60000	0.43650
0.19914	0.87250	0.60000	0.43650
0.16627	1.04700	0.60000	0.43650
0.11861	1.22150	0.60000	0.43650
-0.01682	1.39600	0.60000	0.43650
-0.22971	1.57050	0.60000	0.43650
-0.48223	1.74500	0.60000	0.43650
0.00939	0.00000	0.60000	0.52380
0.07855	0.17450	0.60000	0.52380
0.21985	0.34900	0.60000	0.52380
0.32358	0.52350	0.60000	0.52380
0.30468	0.69800	0.60000	0.52380
0.23907	0.87250	0.60000	0.52380
0.19698	1.04700	0.60000	0.52380
0.10687	1.22150	0.60000	0.52380



FINISHED CONE

FIGURE 1 Illustrating the forming of a sheet metal cone by:
 (a) the spinning process
 (b) spin-forging

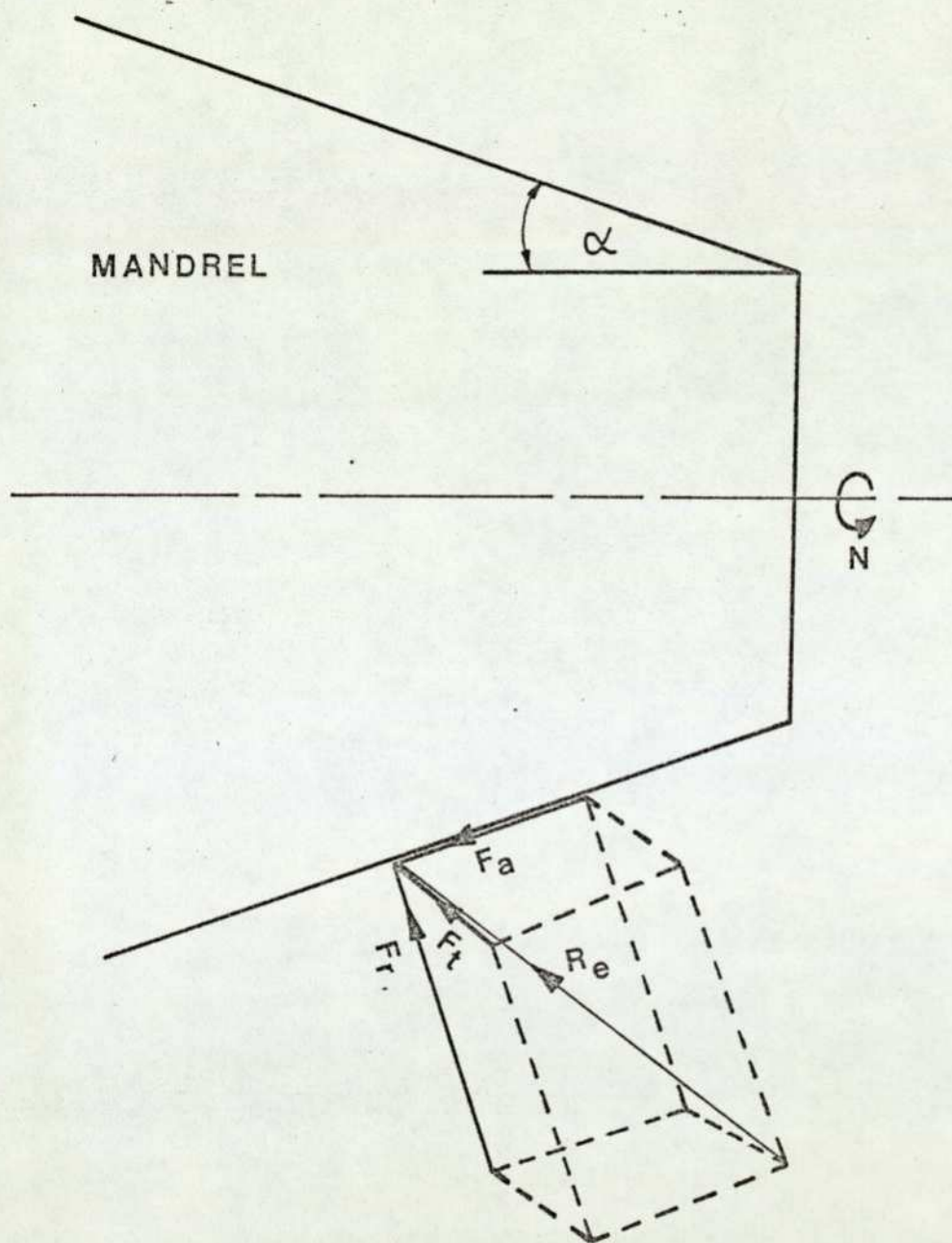


FIGURE 2 Force diagram for spin-forging of cones

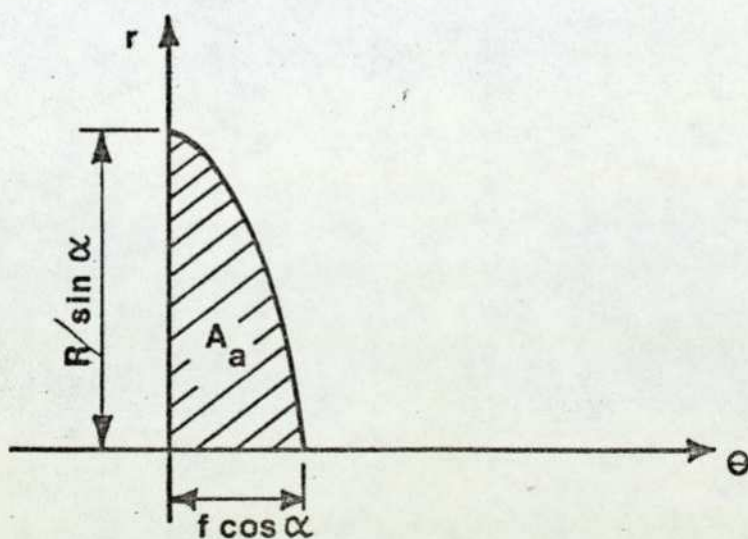
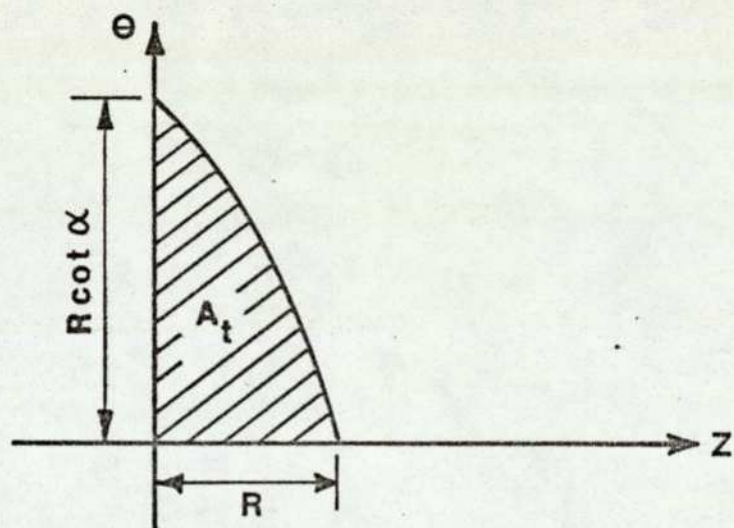
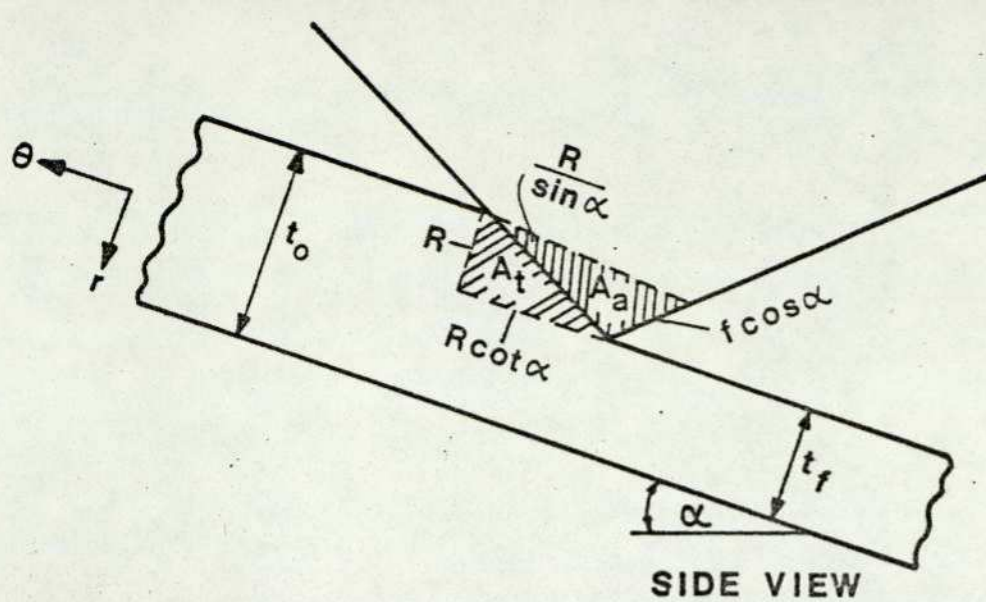


FIGURE 3 Approximate area of contact between the roller and the cone

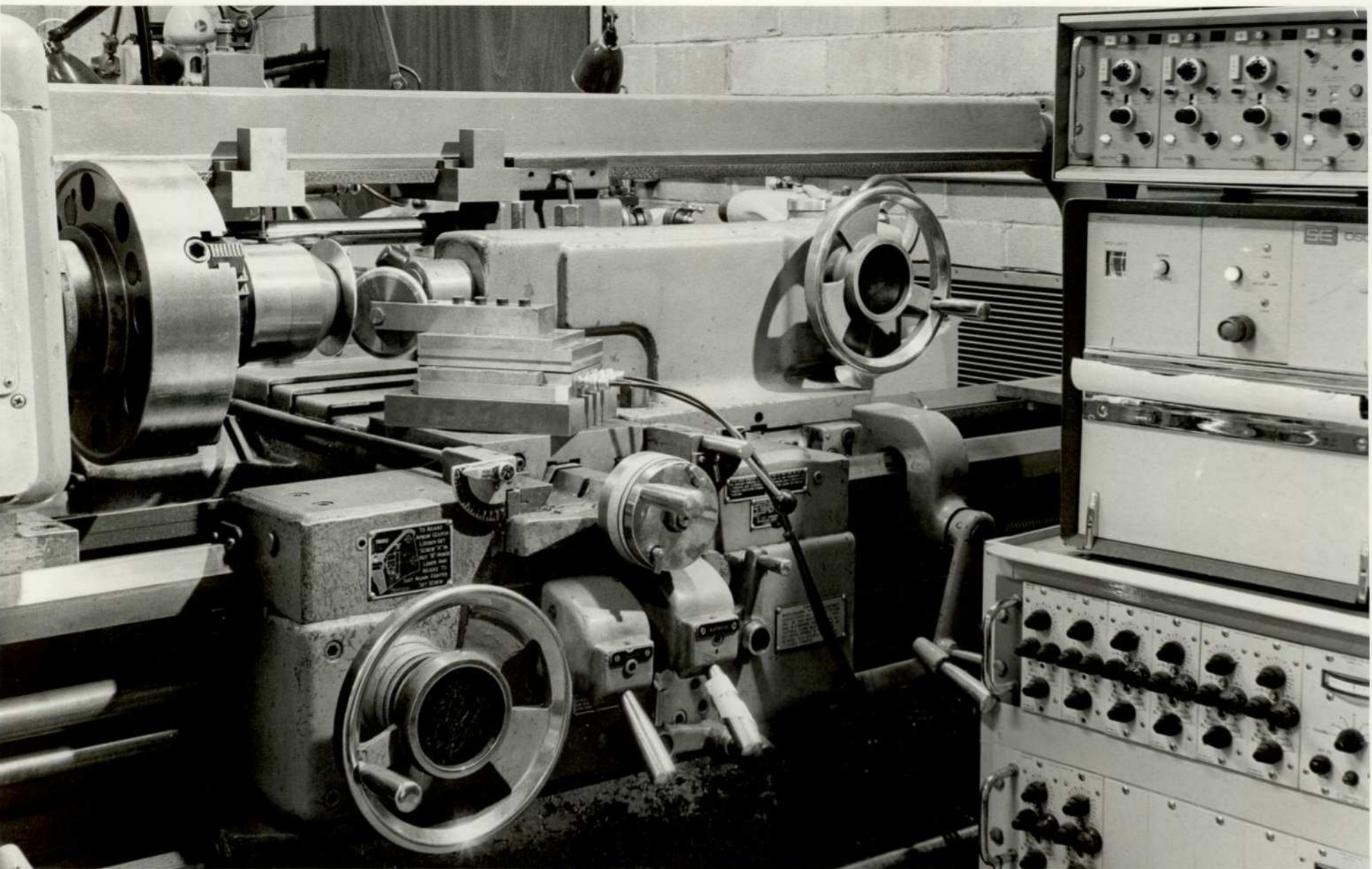


Figure 4. Arrangement of experimental equipment

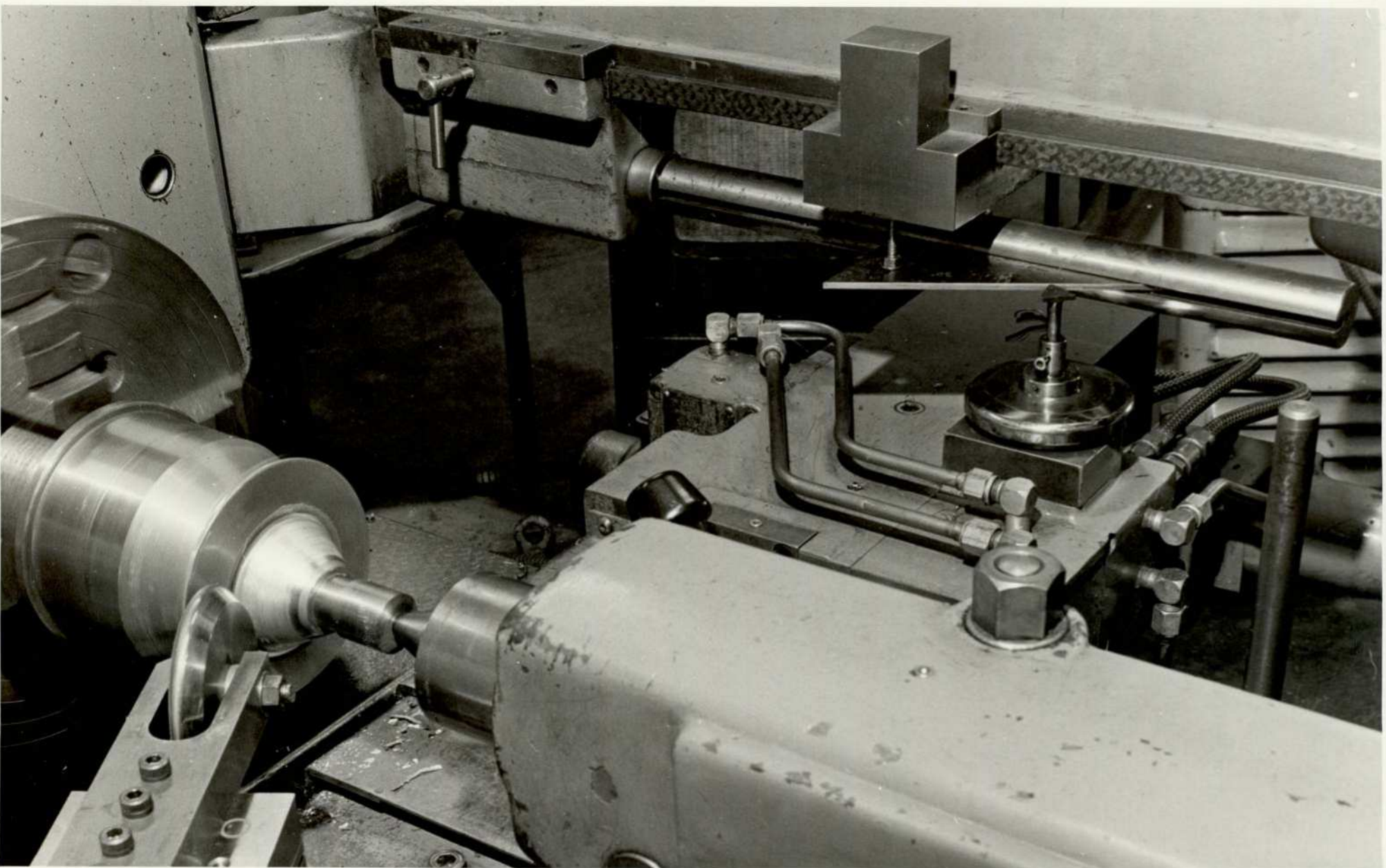


Figure 5 Close up view of the roller, mandrel and template

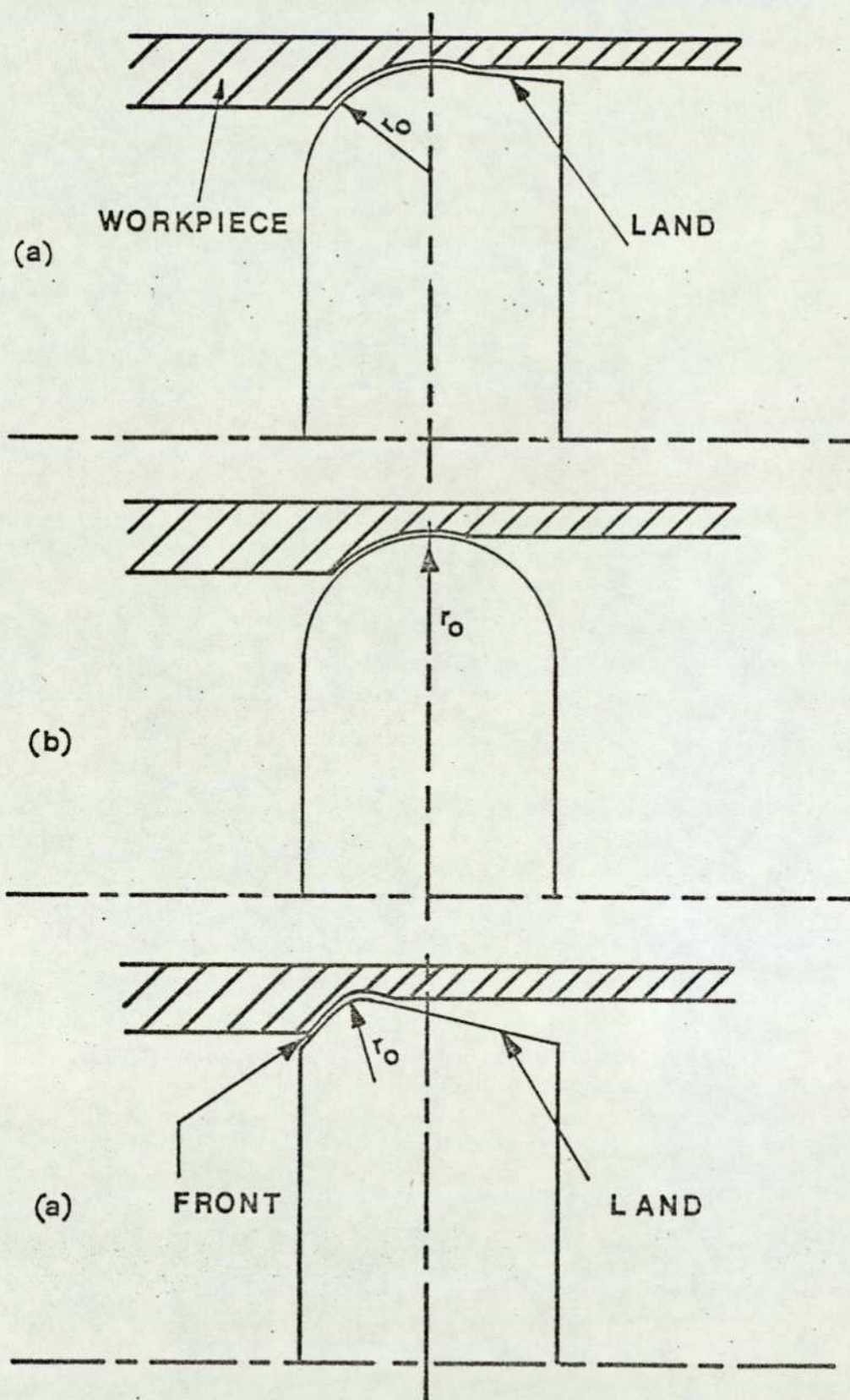


FIGURE 6 The different shapes of the roller

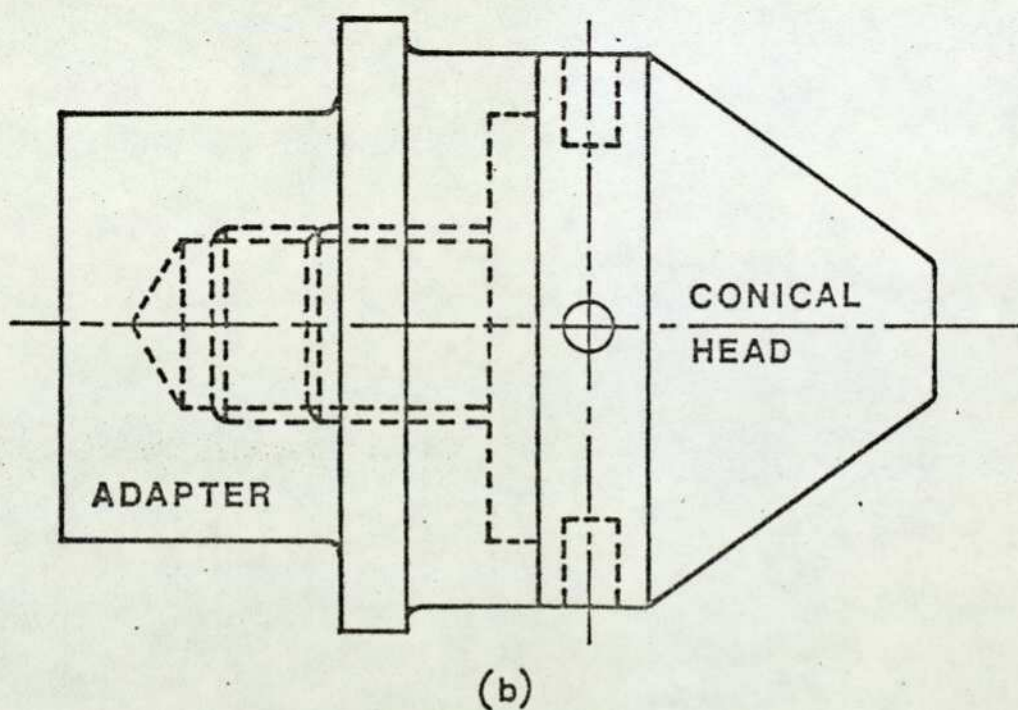
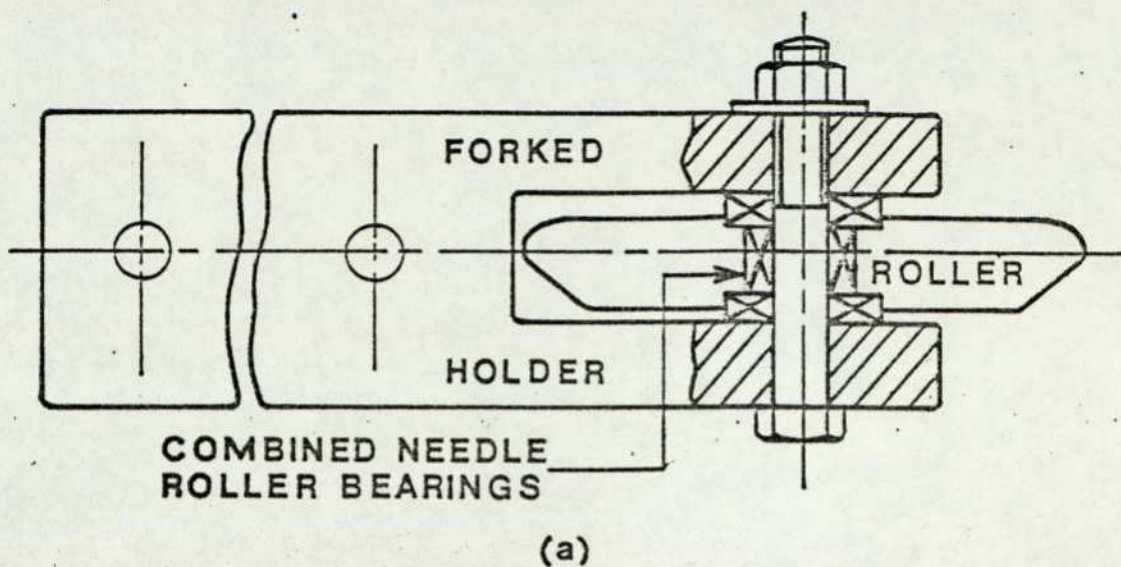


FIGURE 7 (a) The roller assembly mounted in a forked holder for spin-forging

(b) The mandrel showing the adapter and the conical head

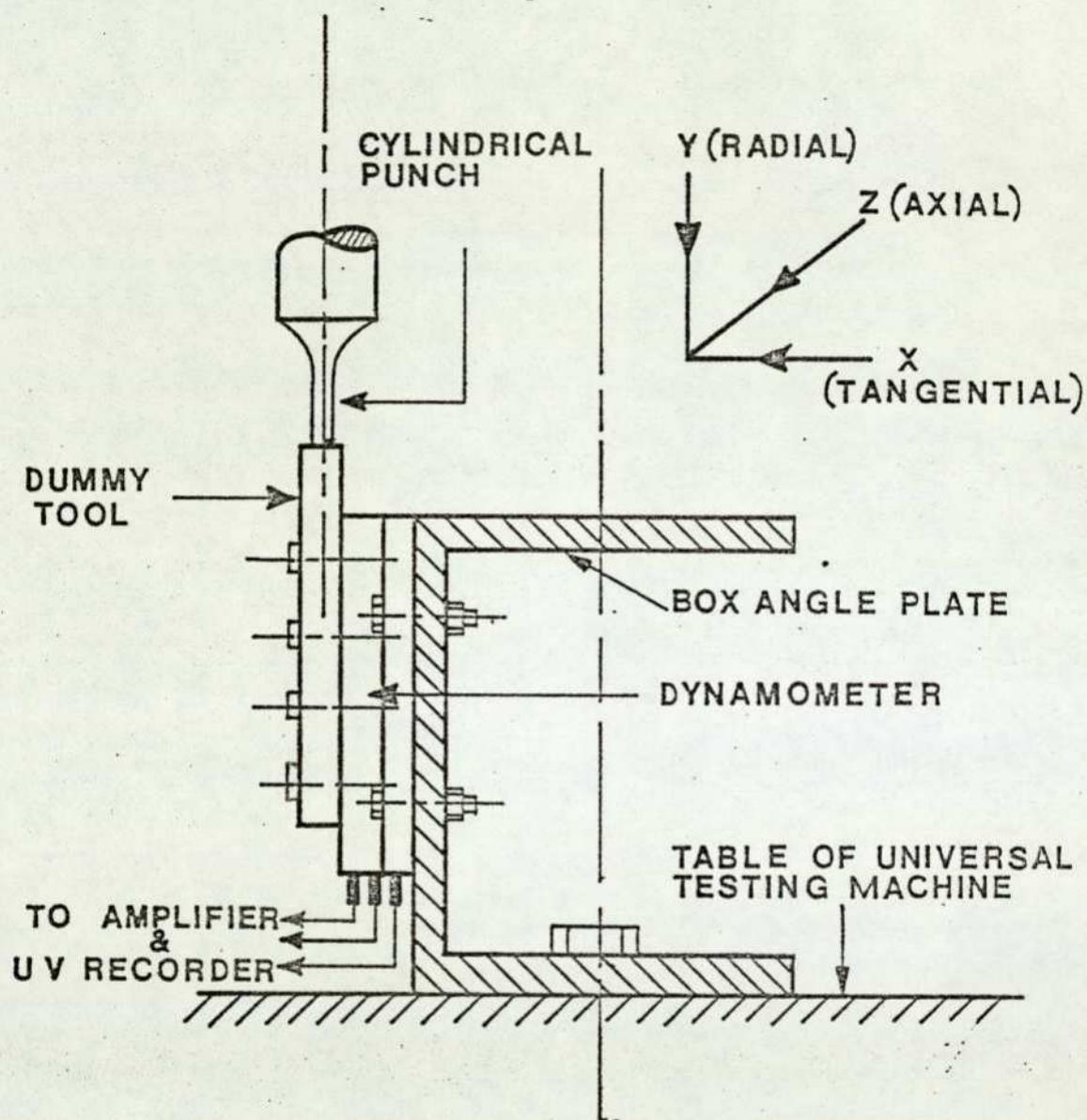


FIGURE 8 Arrangement of calibration equipment

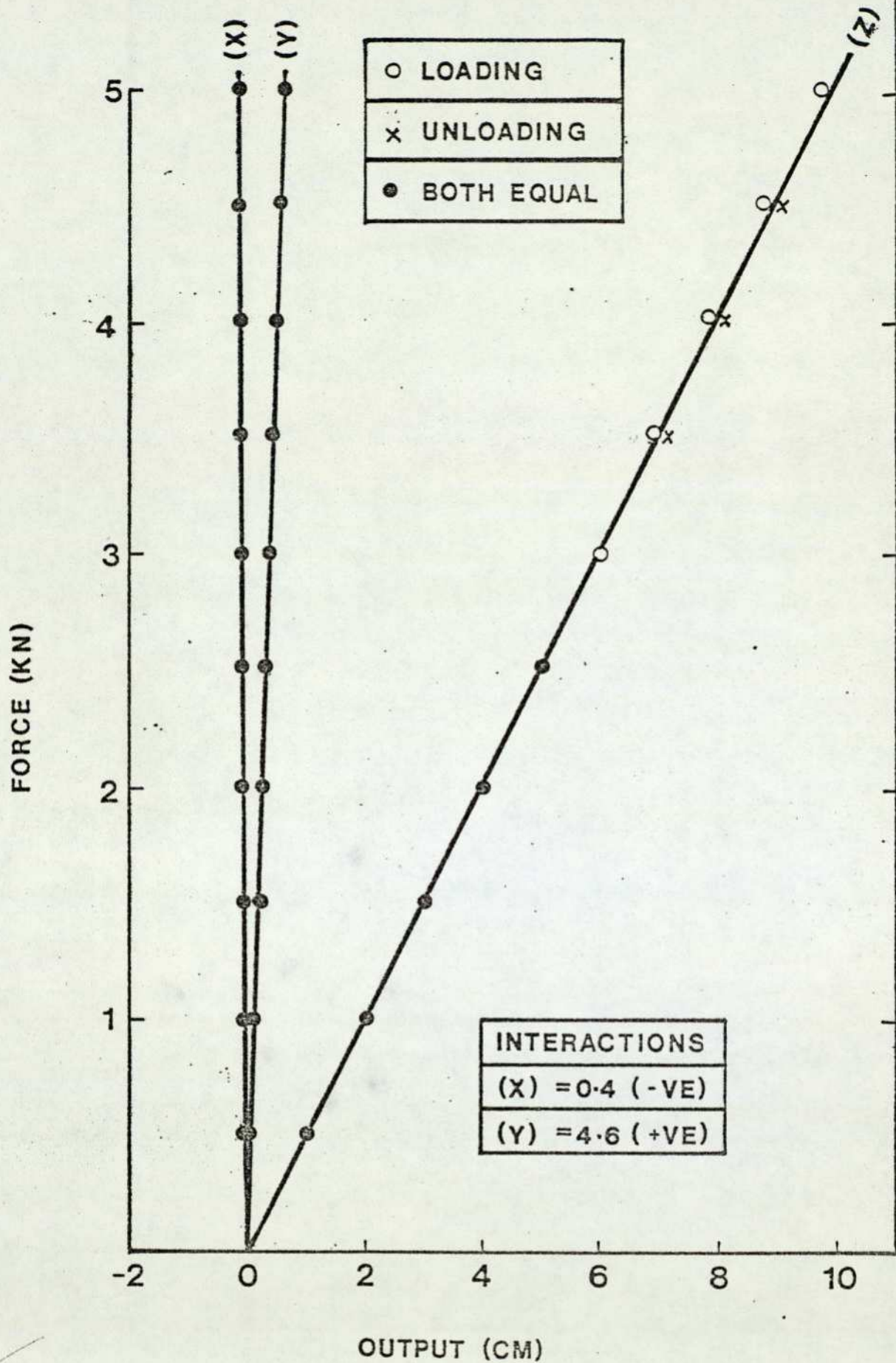


FIGURE 9 Calibration graph loading in Z direction

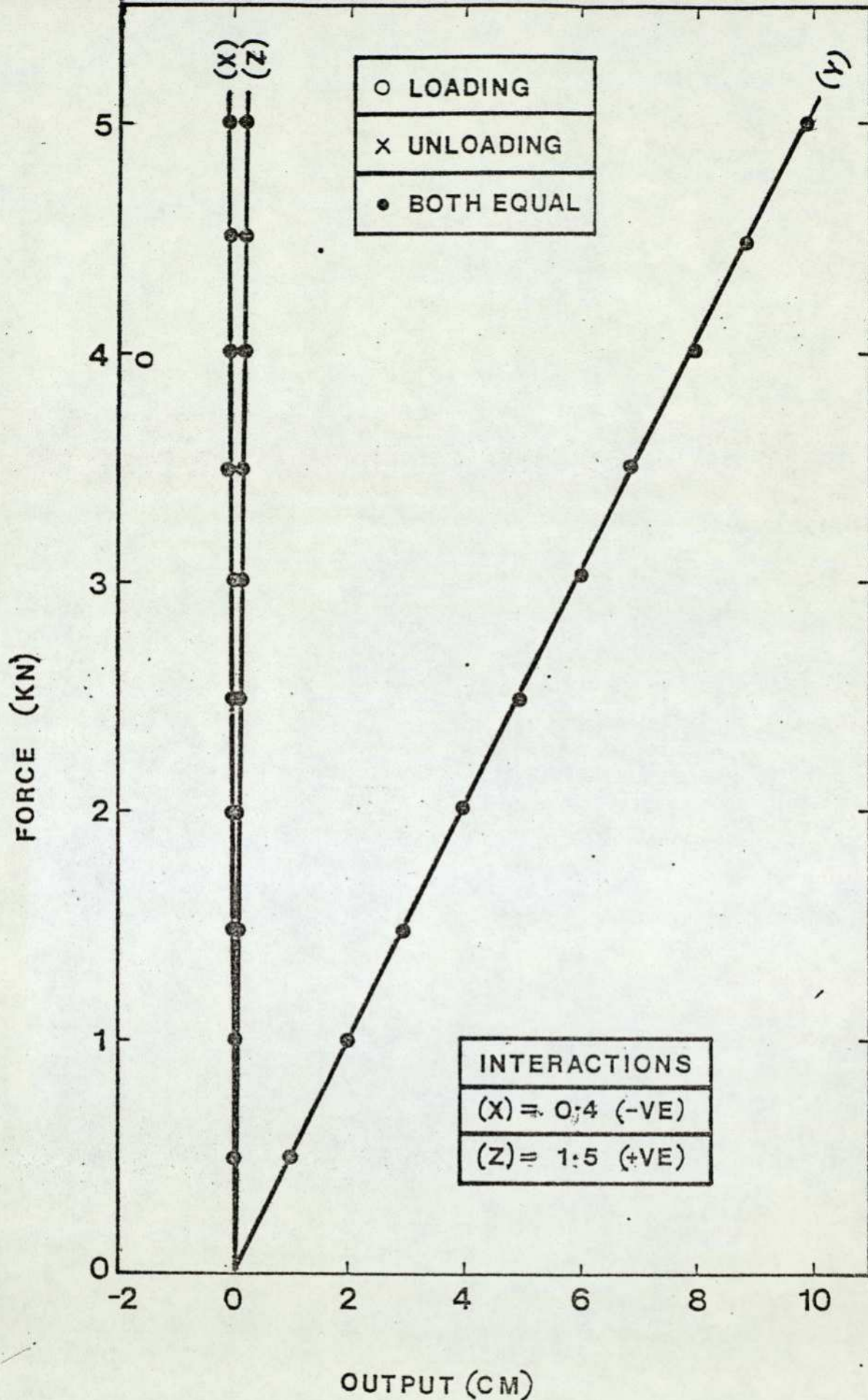


FIGURE 10 Calibration graph loading in Y direction

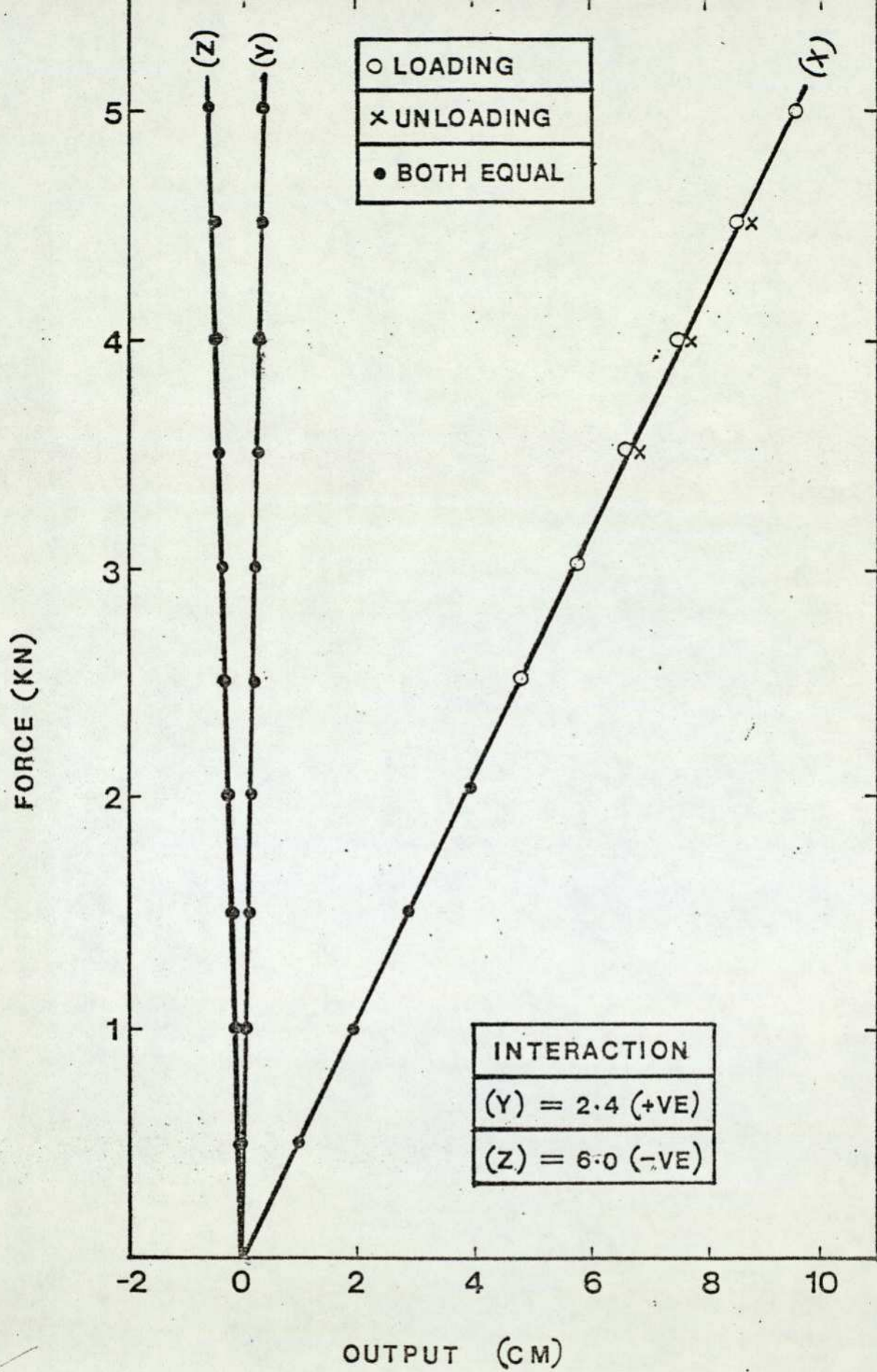


FIGURE 11 Calibration graph loading in X direction

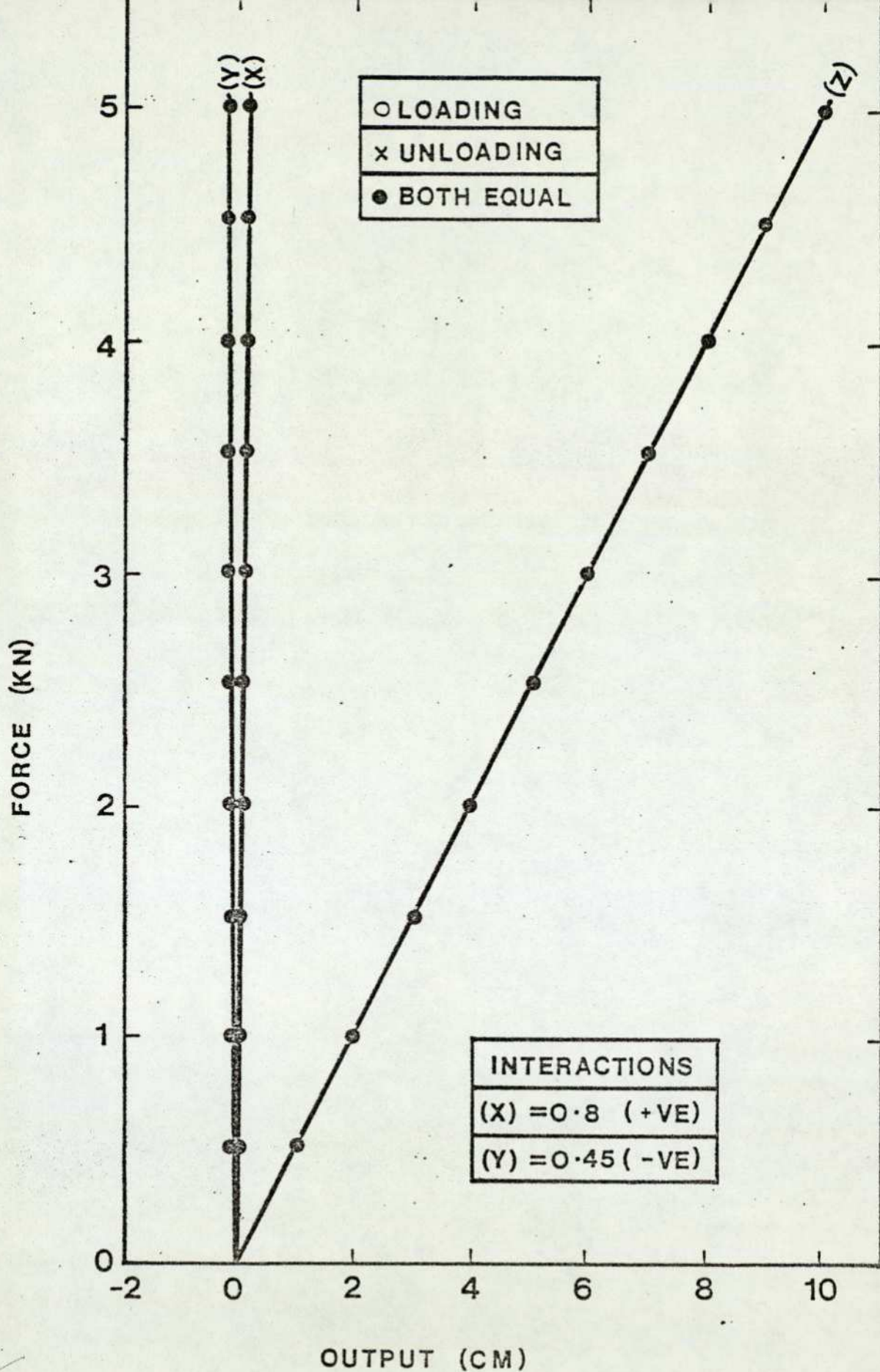


FIGURE 12 Calibration graph loading in centre of the measuring platform of the dynamometer

FIGURE 13 Quasi-static, true compressive stress-compressive natural strain characteristic curve for commercially pure aluminum at ambient temperature

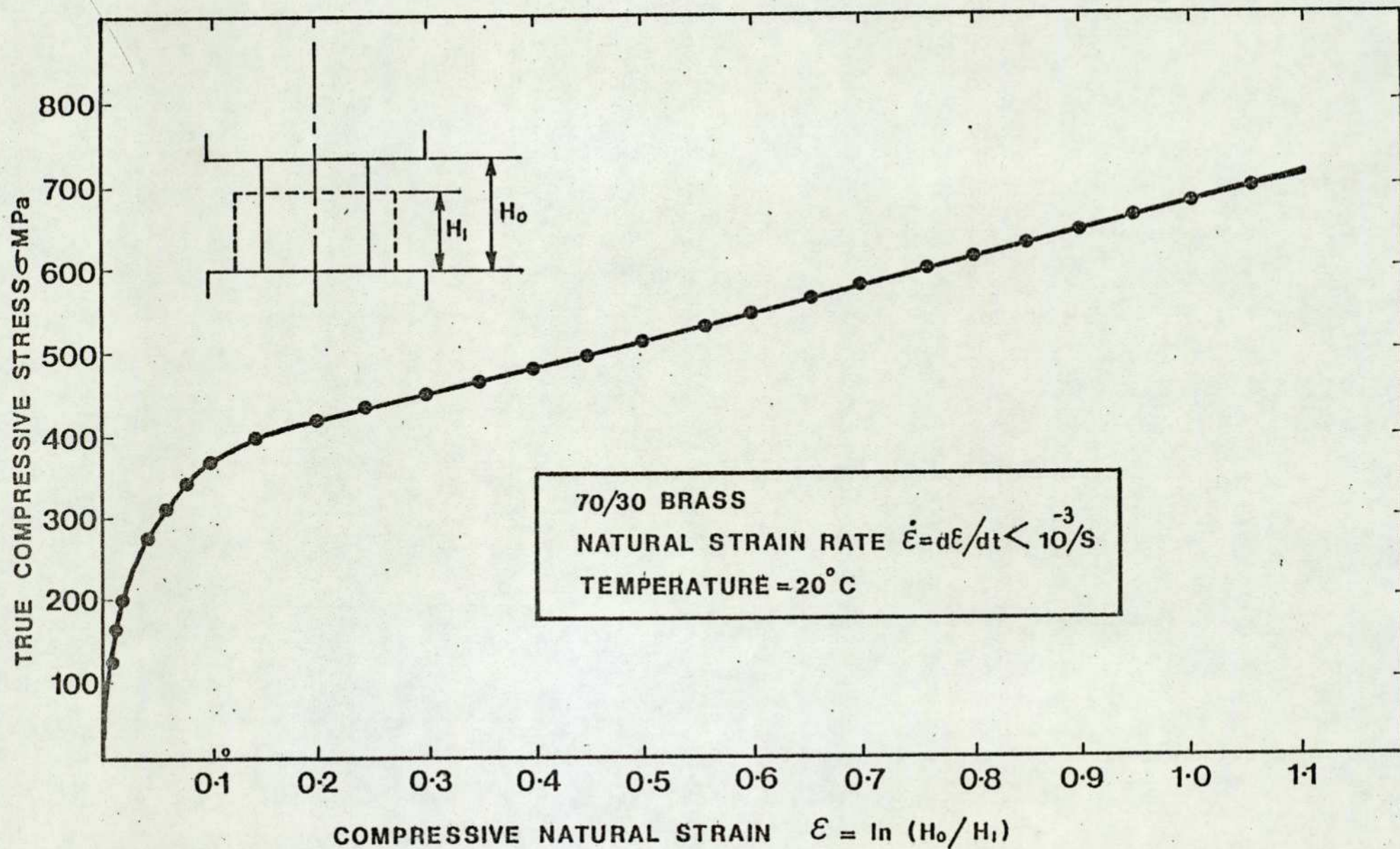


FIGURE 14 Quasi-static, true compressive stress-compressive natural strain characteristic curve for 70/30 brass at ambient temperature

COMPONENTS OF THE RESULTANT FORCE EXERTED
AT THE WORK PIECE ROLLER INTERFACE

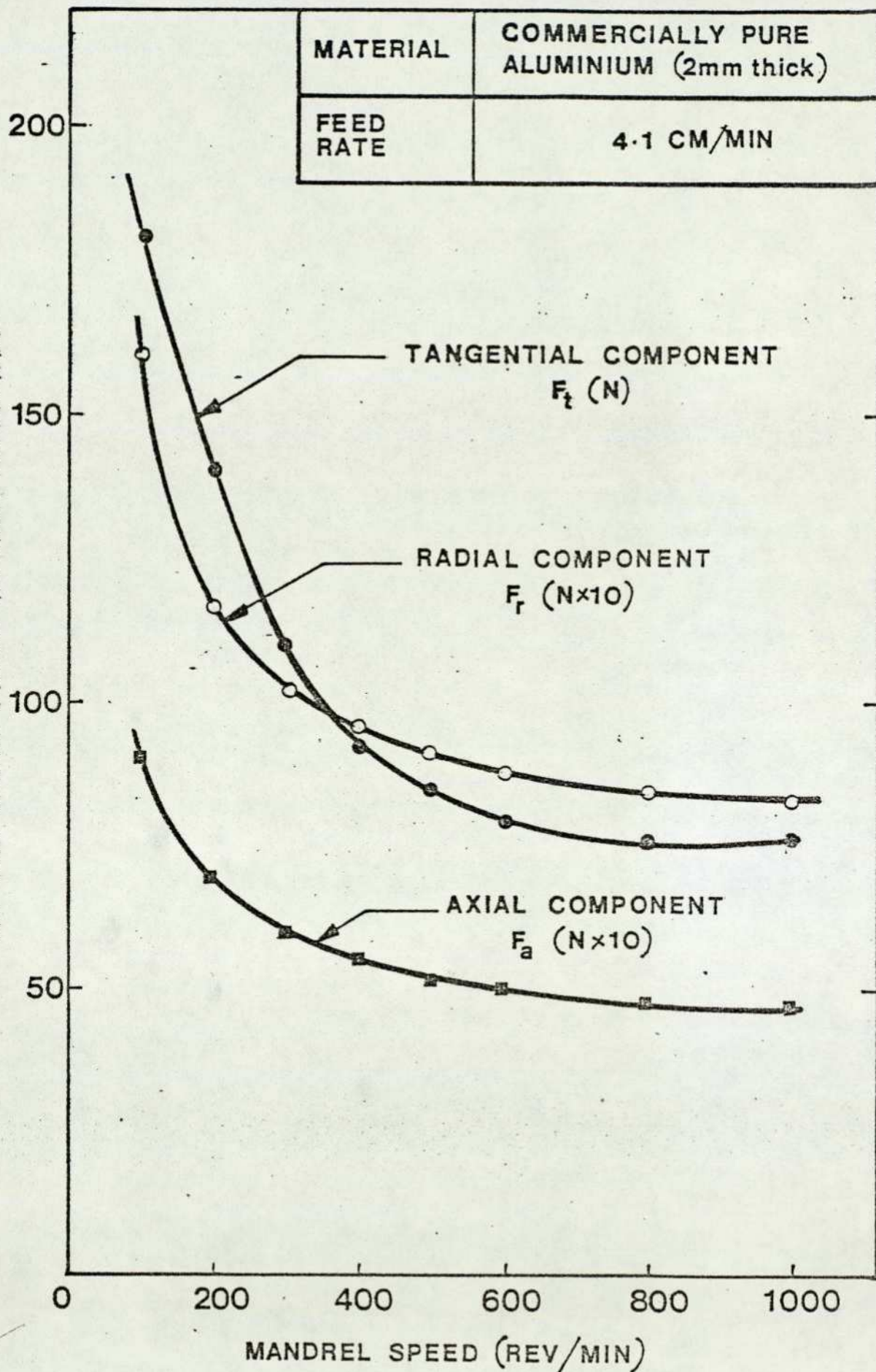


FIGURE 15 Relationship between component forces and mandrel speed for commercially pure aluminium

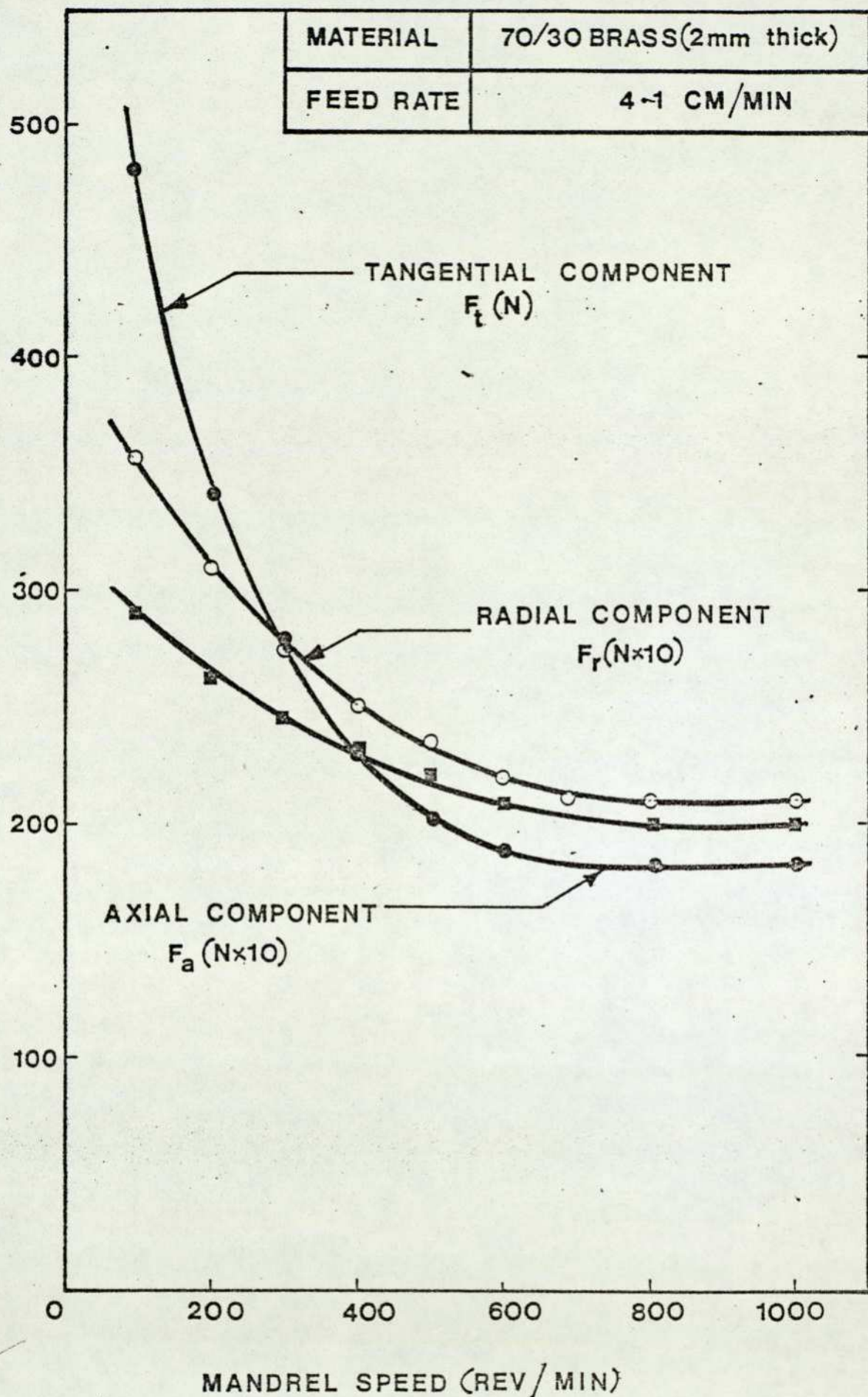


FIGURE 16 Relationship between component forces and mandrel speed for 70/30 brass

MATERIAL

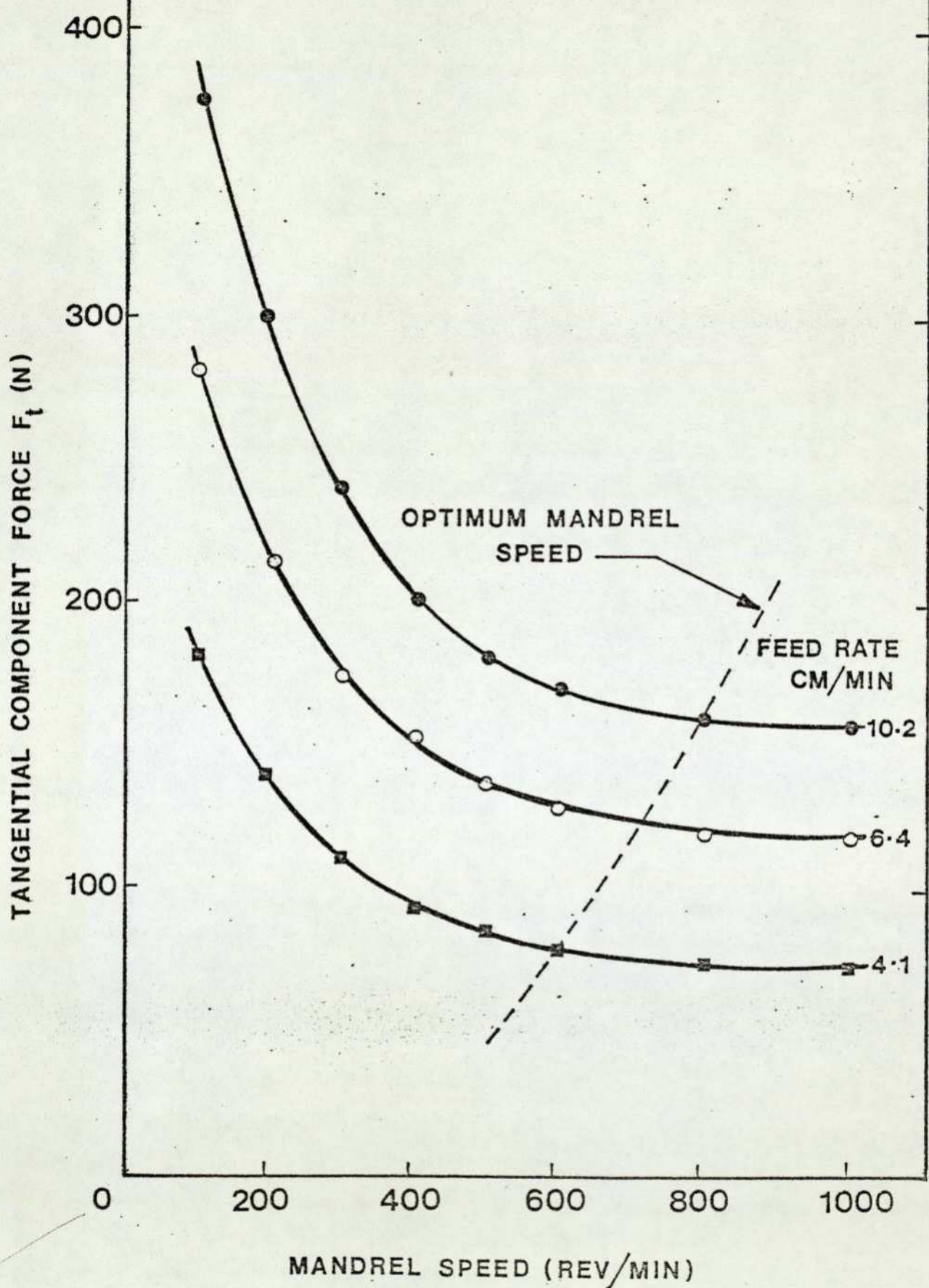
COMMERCIALLY PURE
ALUMINIUM (2mm thick)

FIGURE 17 Relationship between tangential component force and mandrel speed at various feed rates for commercially pure aluminium

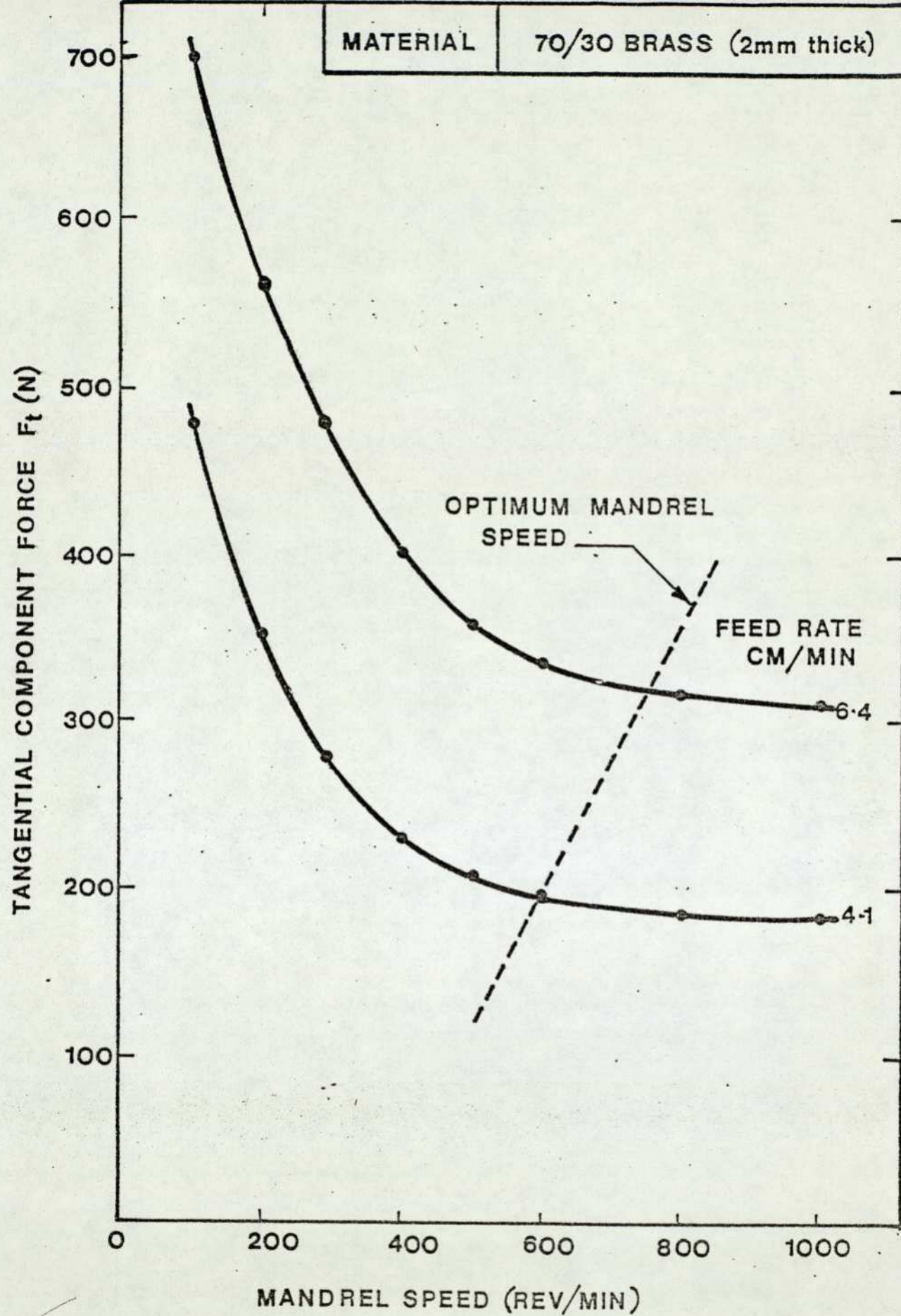


FIGURE 18 Relationship between tangential component force and mandrel speed at various feed rates for 70/30 brass

COMPONENT IS OF THE RESULT, IT FORCE EXERTED

AT THE WORKPIECE-ROLLER INTERFACE

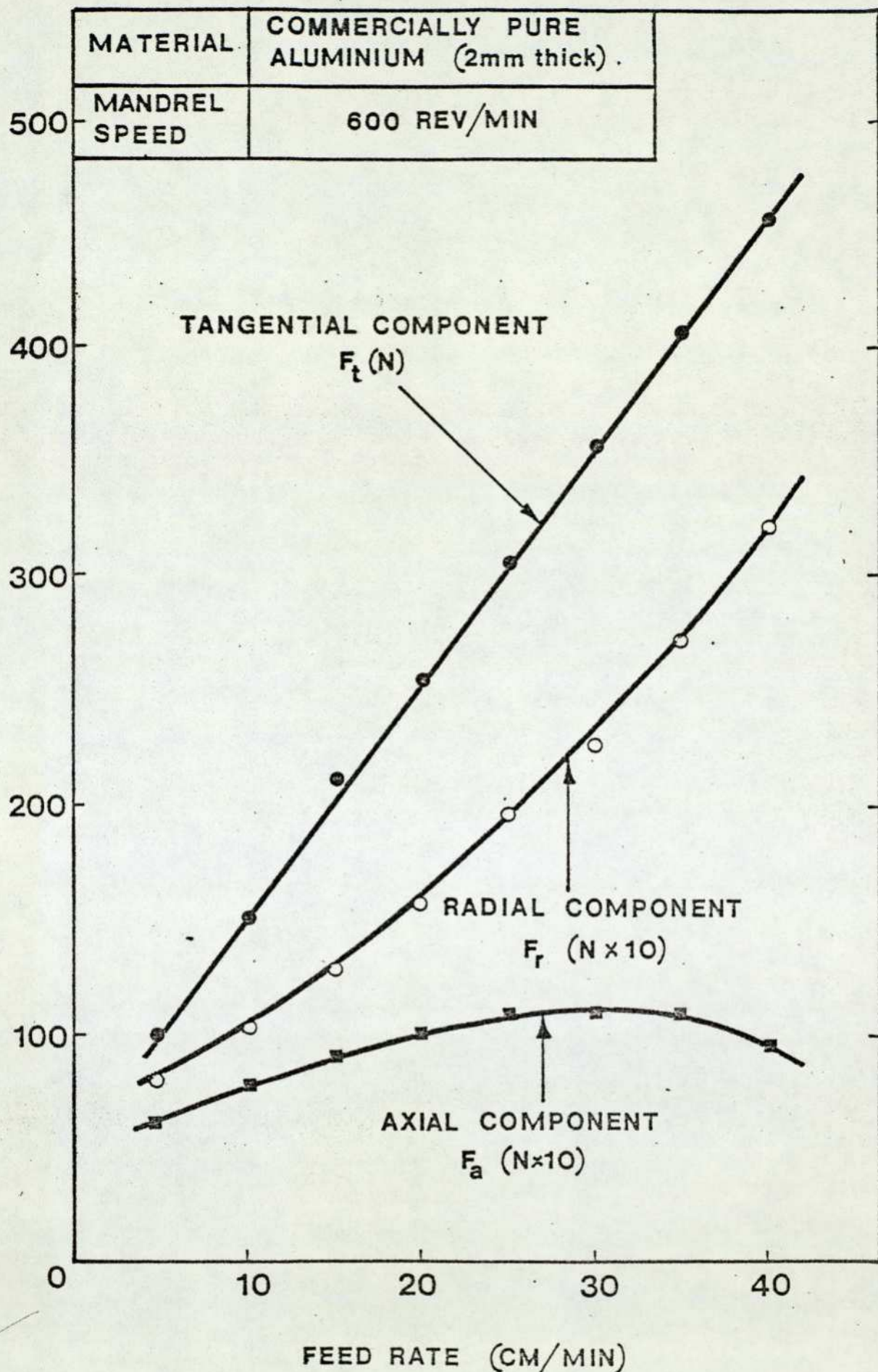


FIGURE 19 Relationship between component forces and feed rate for commercially pure aluminium

COMPONENT OF THE RESULTANT FORCE EXERTED
AT THE WORKPIECE-ROLLER INTERFACE

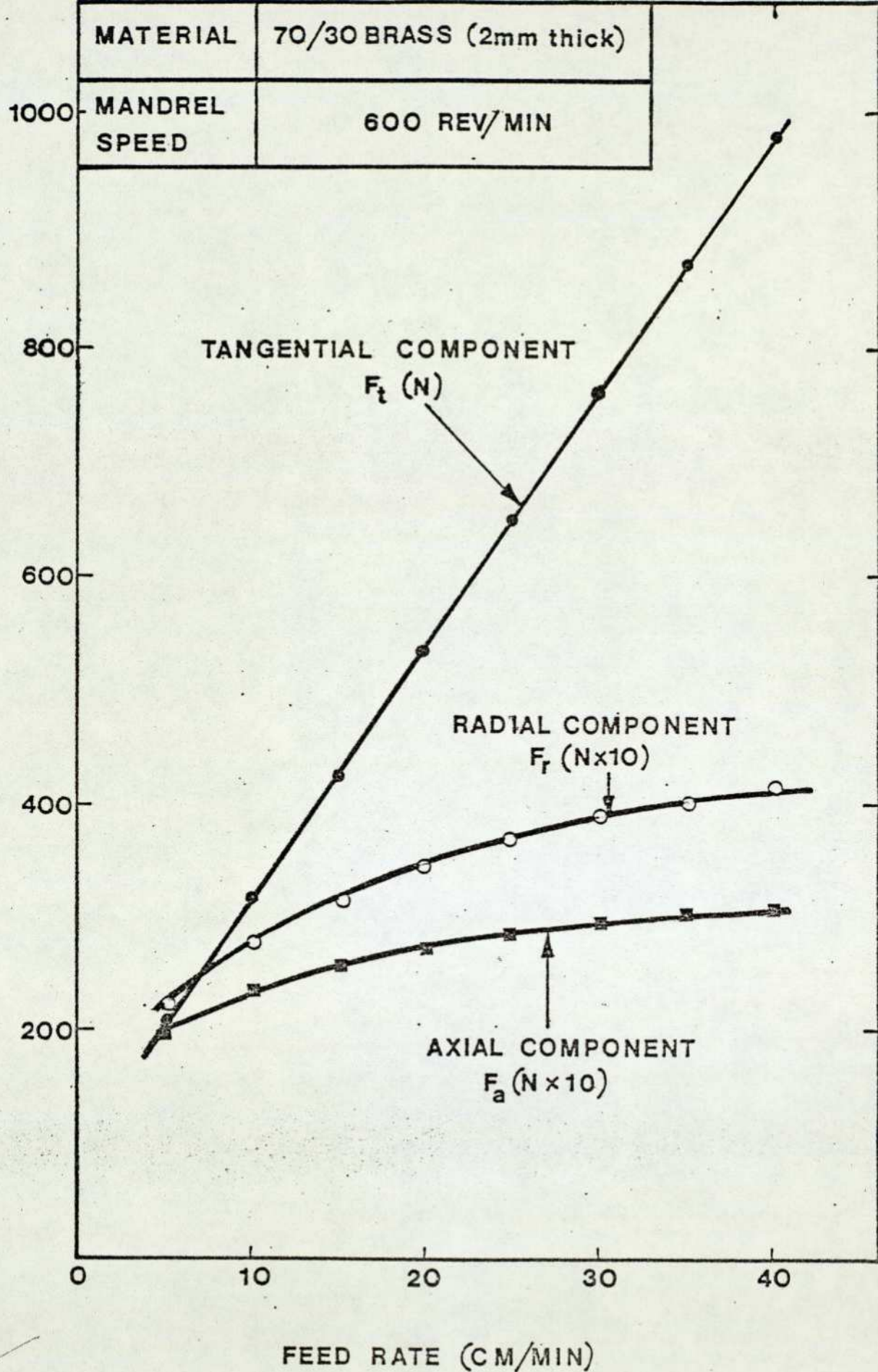


FIGURE 20 Relationship between component forces and feed rate
for 70/30 brass

COMPONENTS OF THE RESULTANT FORCE EXERTED AT
THE WORKPIECE-ROLLER INTERFACE

MATERIAL	COMMERCIALLY PURE ALUMINIUM (2mm thick)
MANDREL SPEED	600 REV/MIN
FEED RATE	6,4 CM/MIN

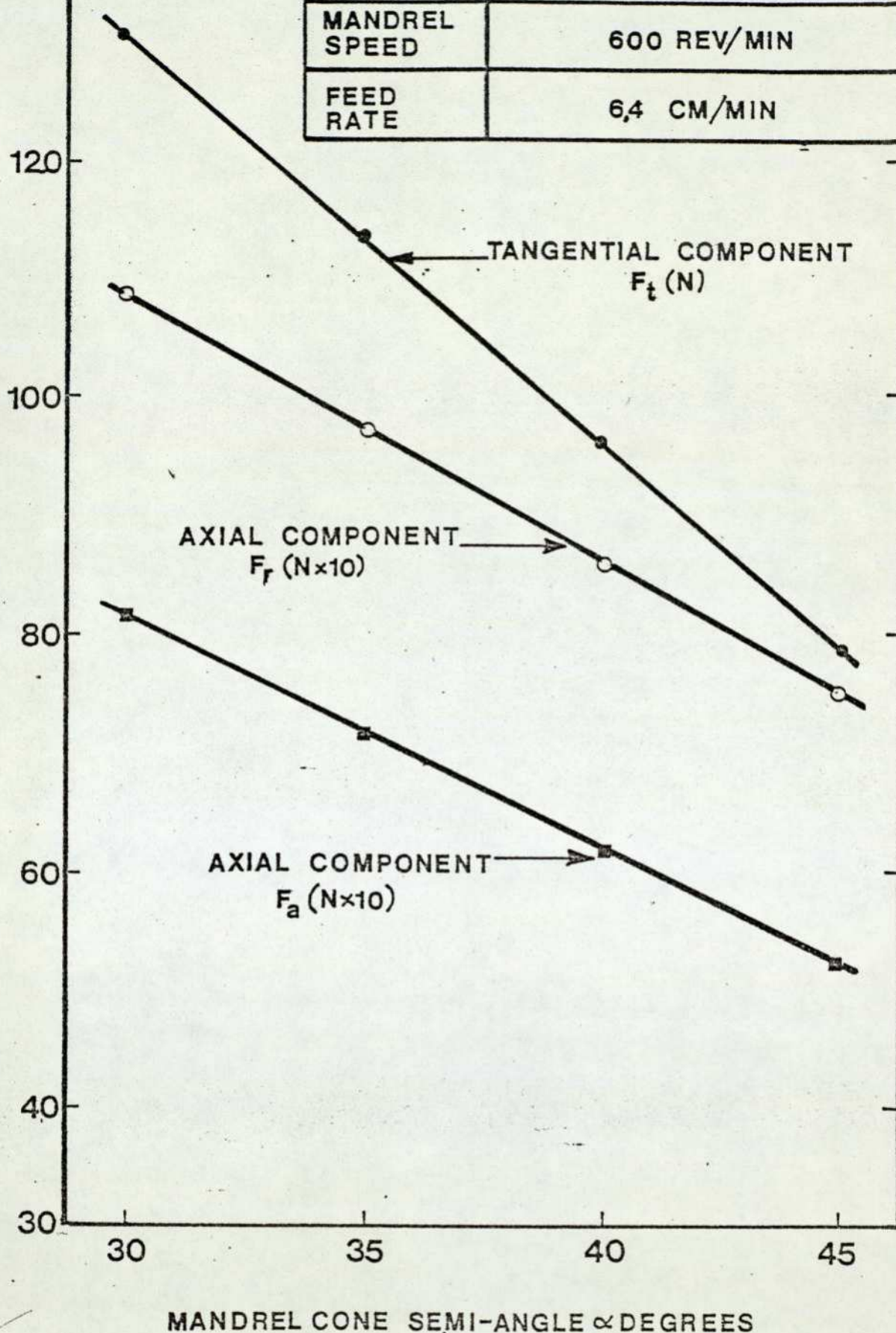


FIGURE 21 Relationship between component forces and cone semi-angle for commercially pure aluminium

COMPONENTS OF THE RESULTANT FORCE EXERTED
AT THE WORKPIECE-ROLLER INTERFACE

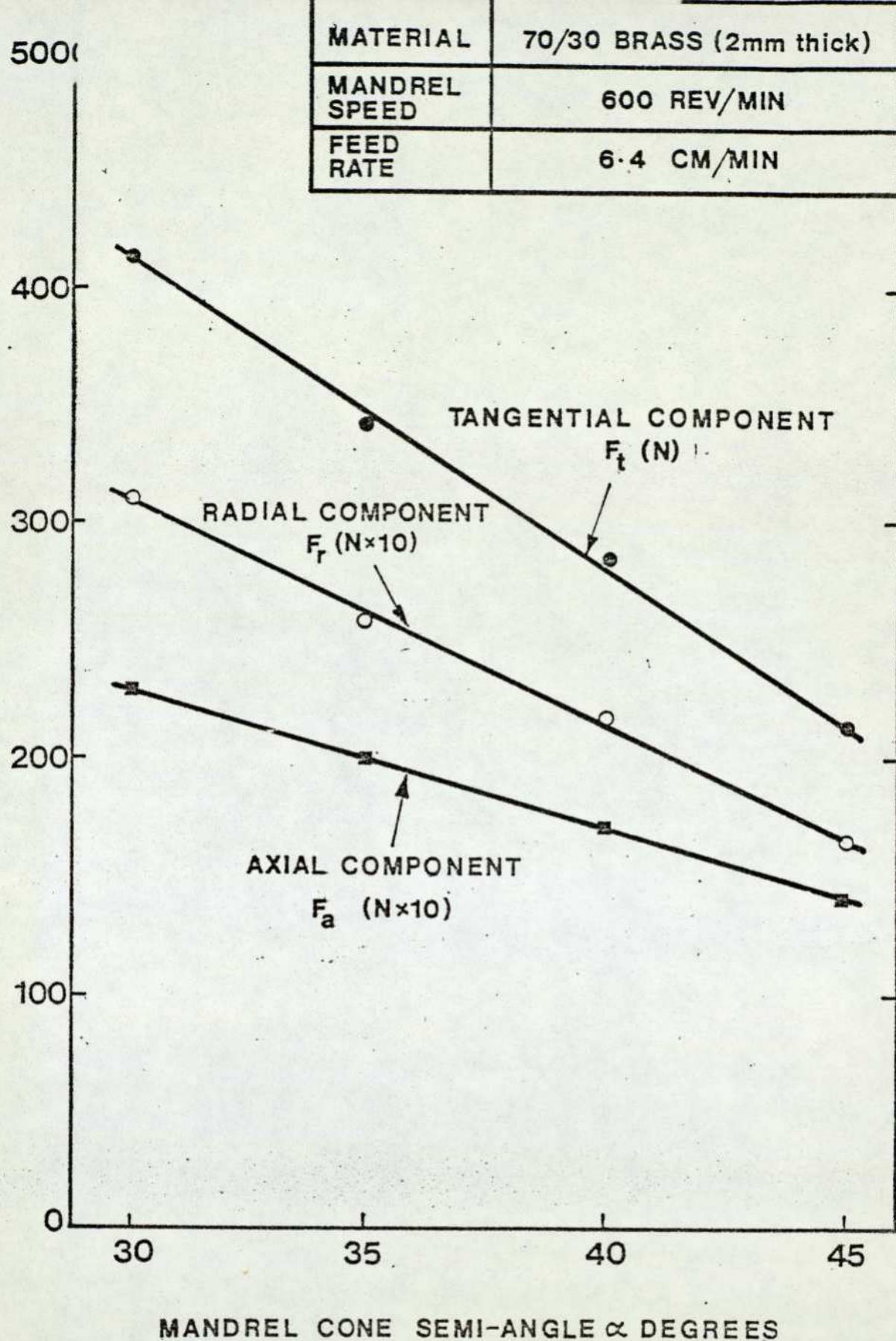


FIGURE 22 Relationship between component forces and cone semi-angle for 70/30 brass

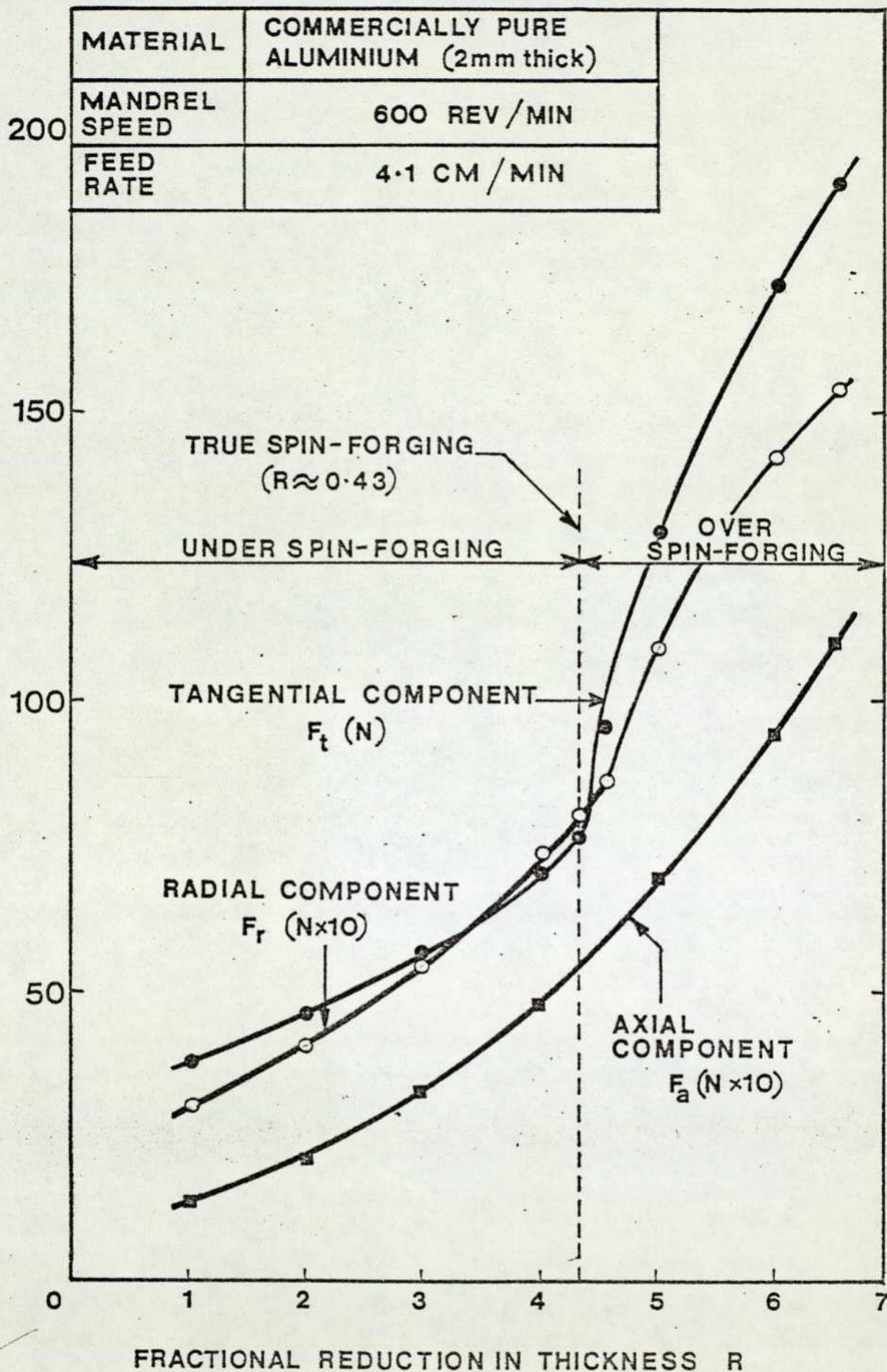


FIGURE 23 Relationship between component forces and fractional reduction in wall thickness for commercially pure aluminium ($\alpha = 35^\circ$)

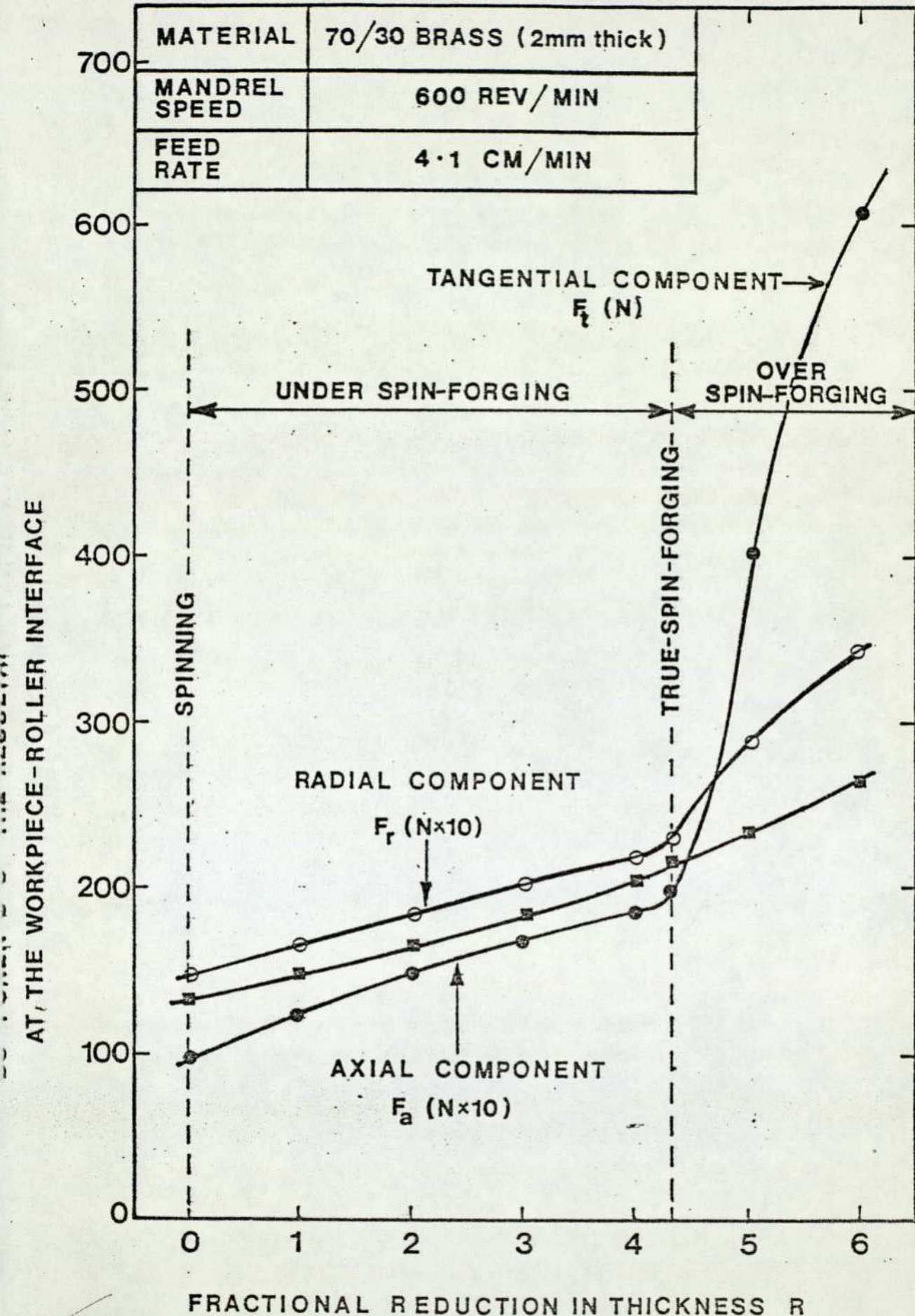


FIGURE 24 Relationship between component forces and fractional reduction in wall thickness for 70/30 brass ($\alpha=35^\circ$)

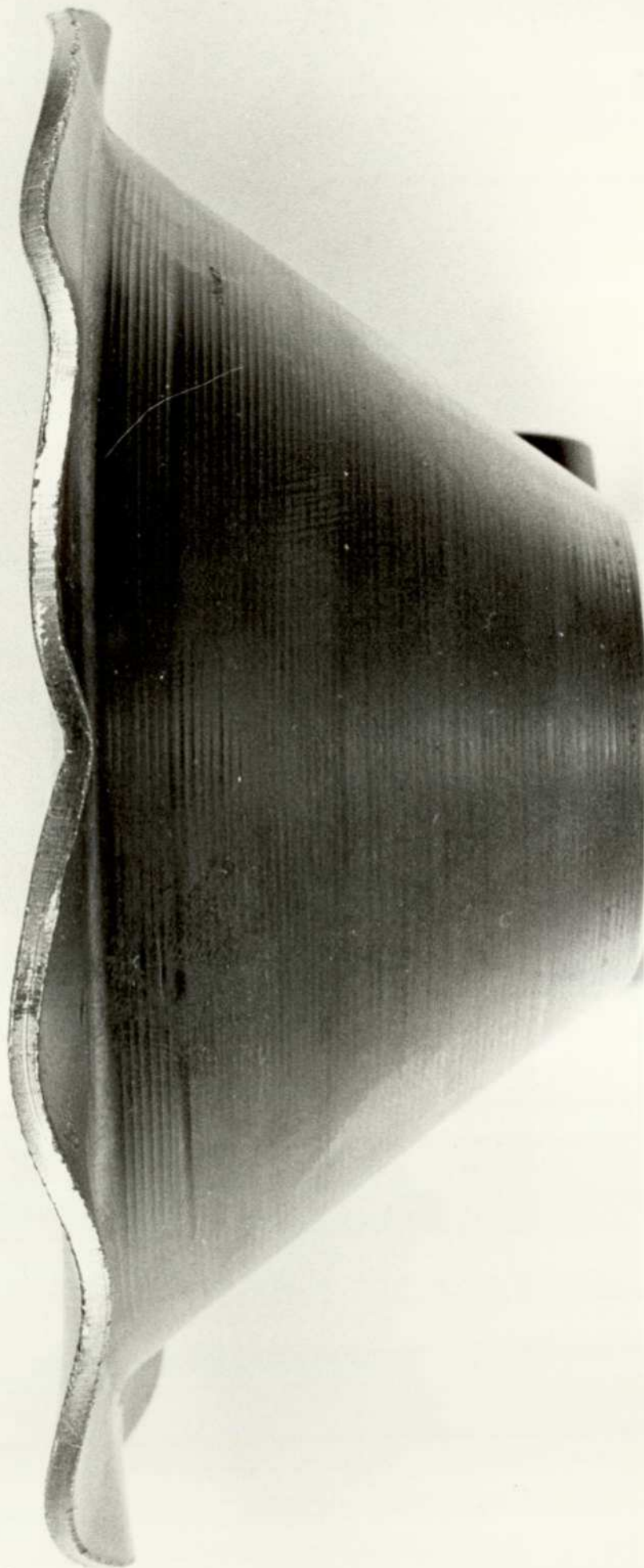


Figure 25 Wrinkling of the flange

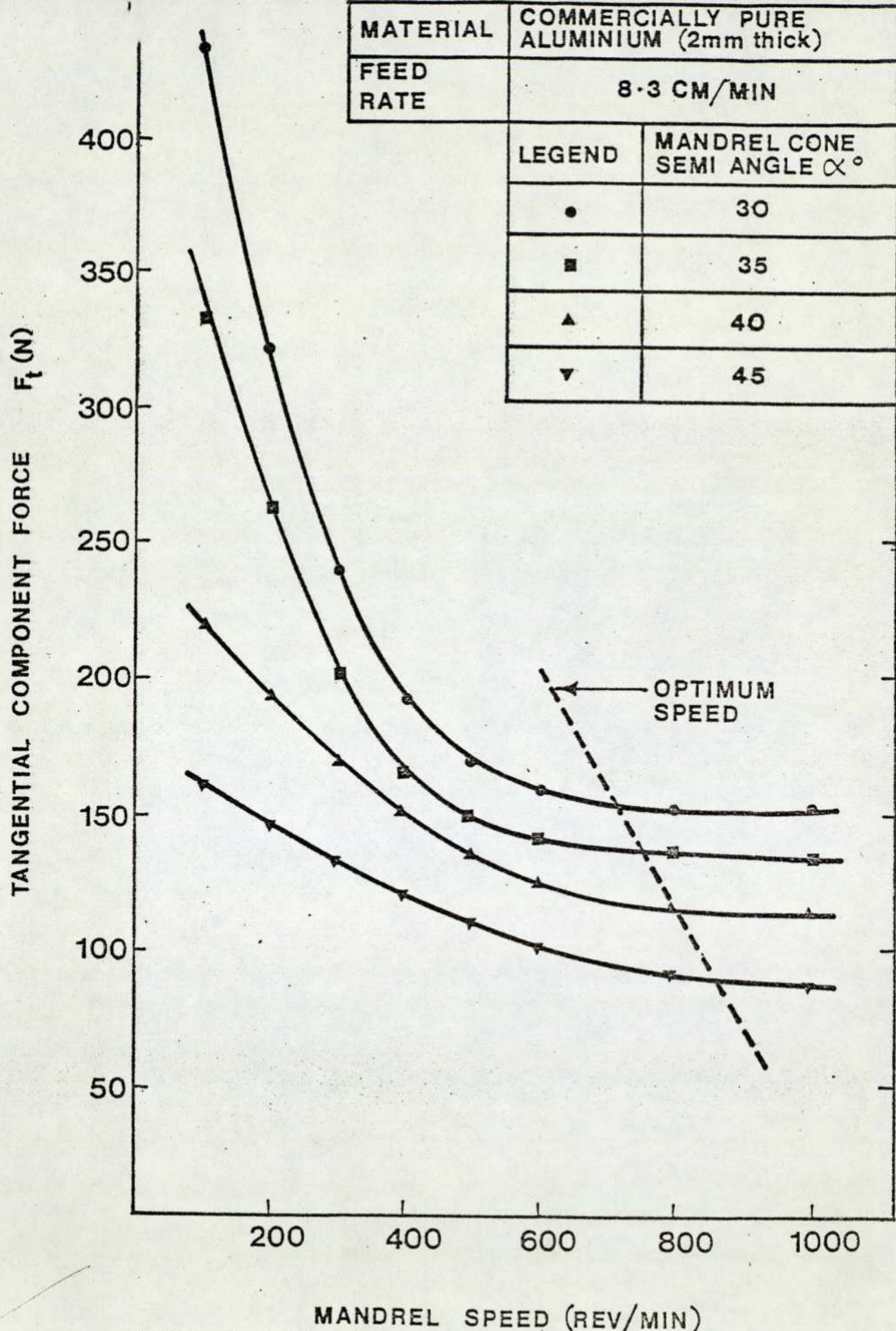


FIGURE 26 Relationship between tangential component force and mandrel speed at various cone semi-angles for commercially pure aluminium

MATERIAL	70/30 BRASS (2mm thick)	
FEED RATE	6.4 CM/MIN	
LEGEND	MANDREL CONE SEMI ANGLE α°	
●	30	
■	35	
▲	40	
▼	45	

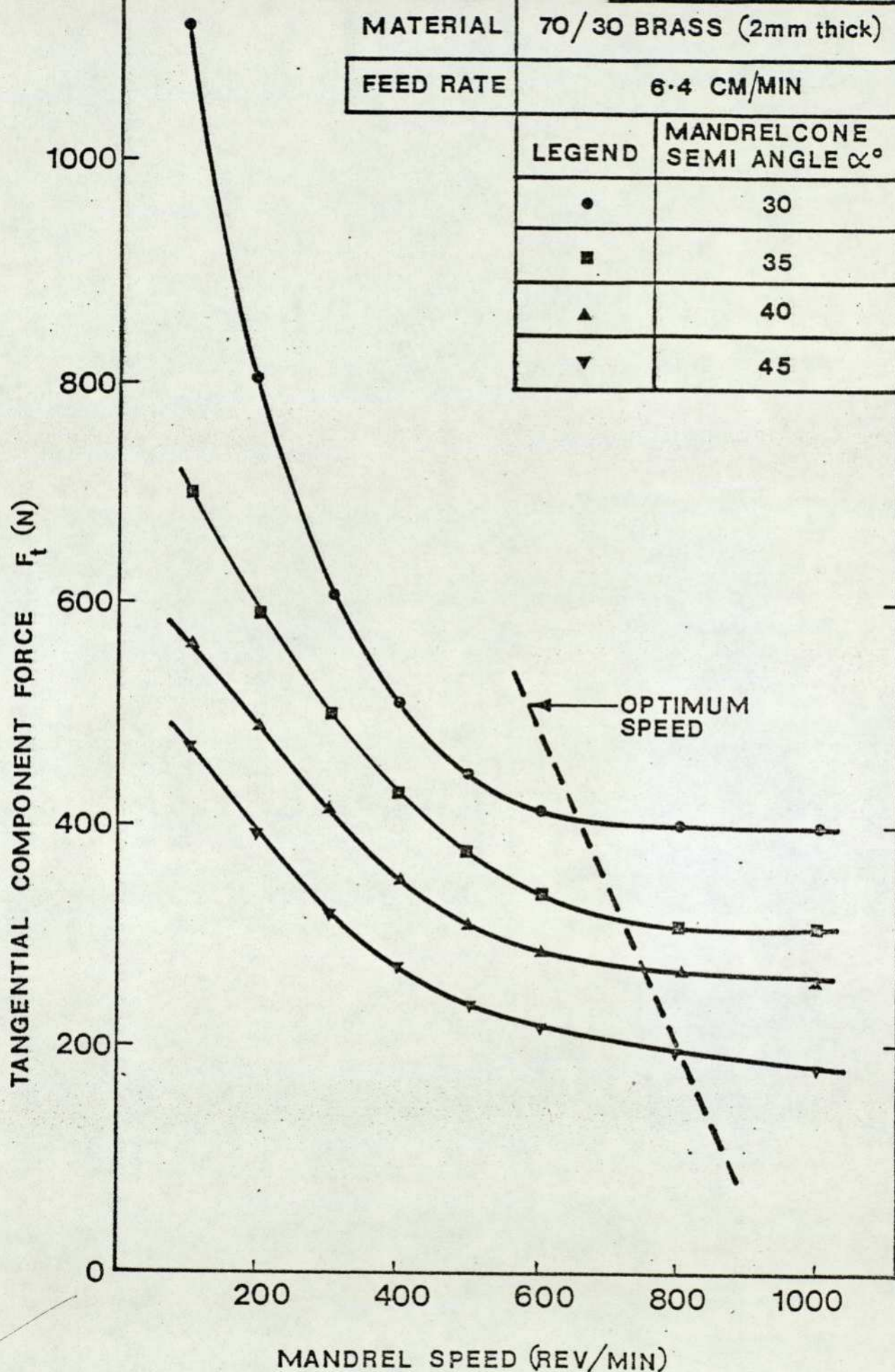


FIGURE 27 Relationship between tangential component force and mandrel speed at various cone semi-angles for 70/30 brass

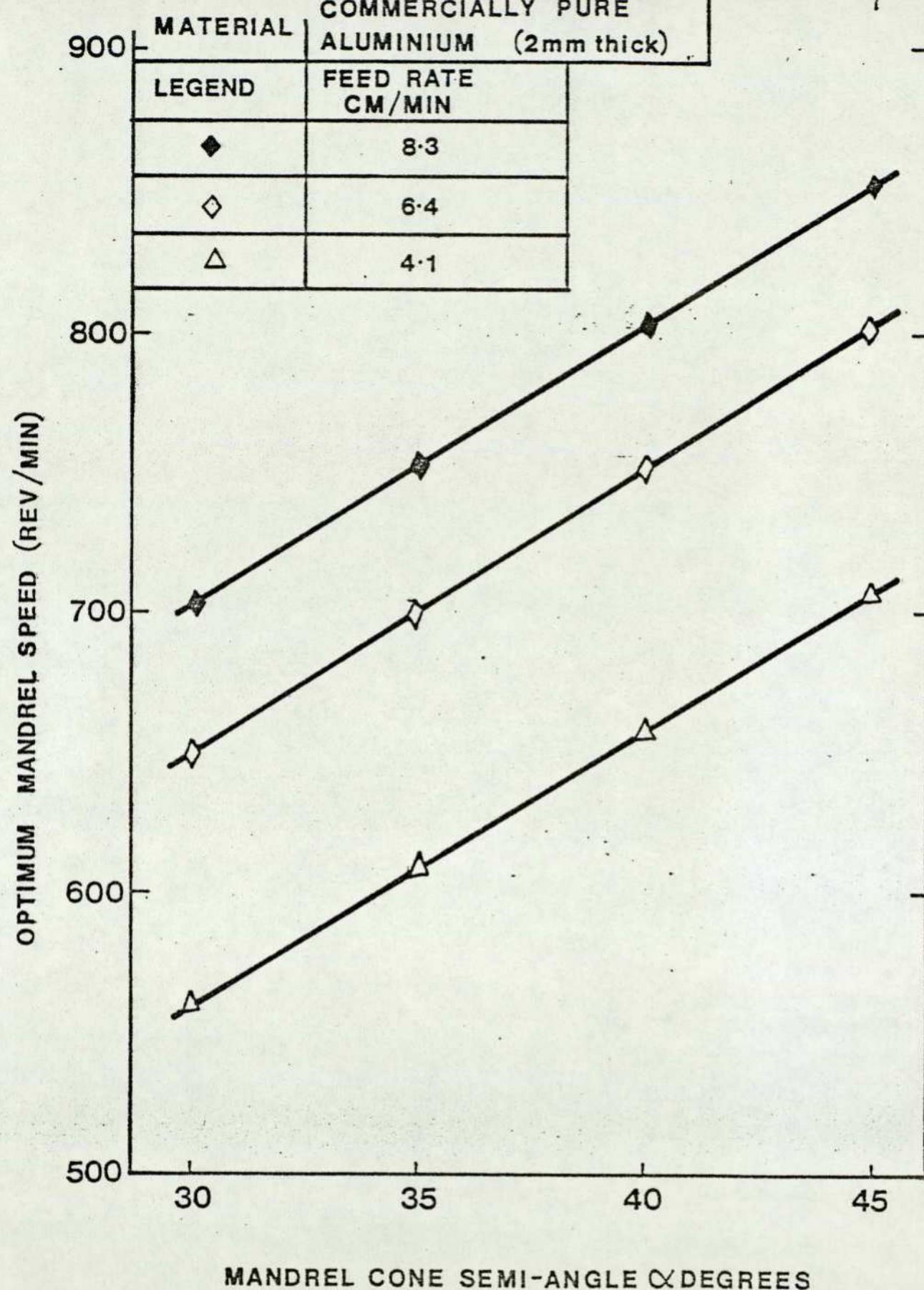


FIGURE 28 Relationship between optimum mandrel speed and cone semi angle at different feed rates for commercially pure aluminium

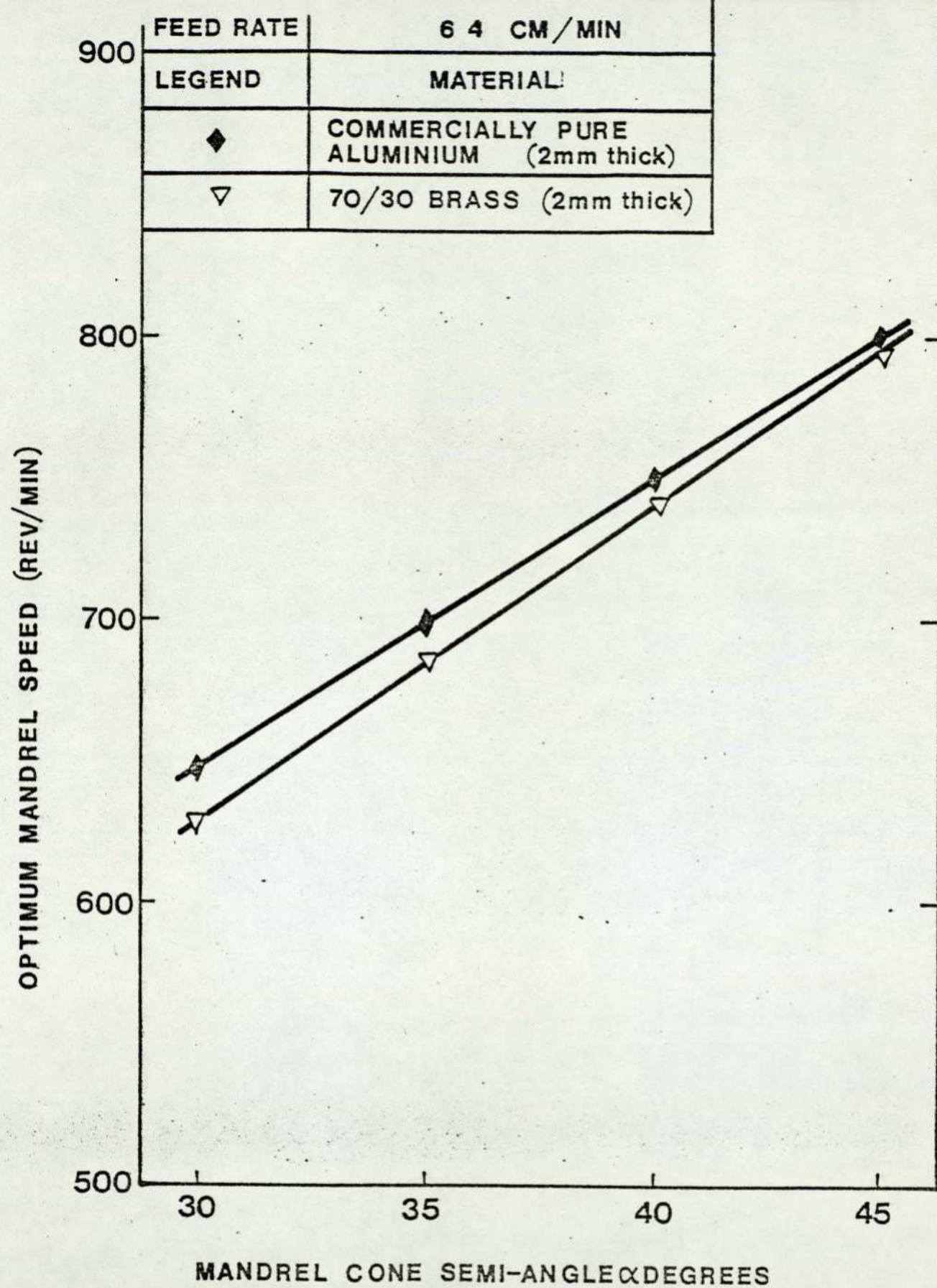


FIGURE 29 Relationship between optimum mandrel speed and cone semi-angle at constant feed rate for commercially pure aluminium and 70/30 brass

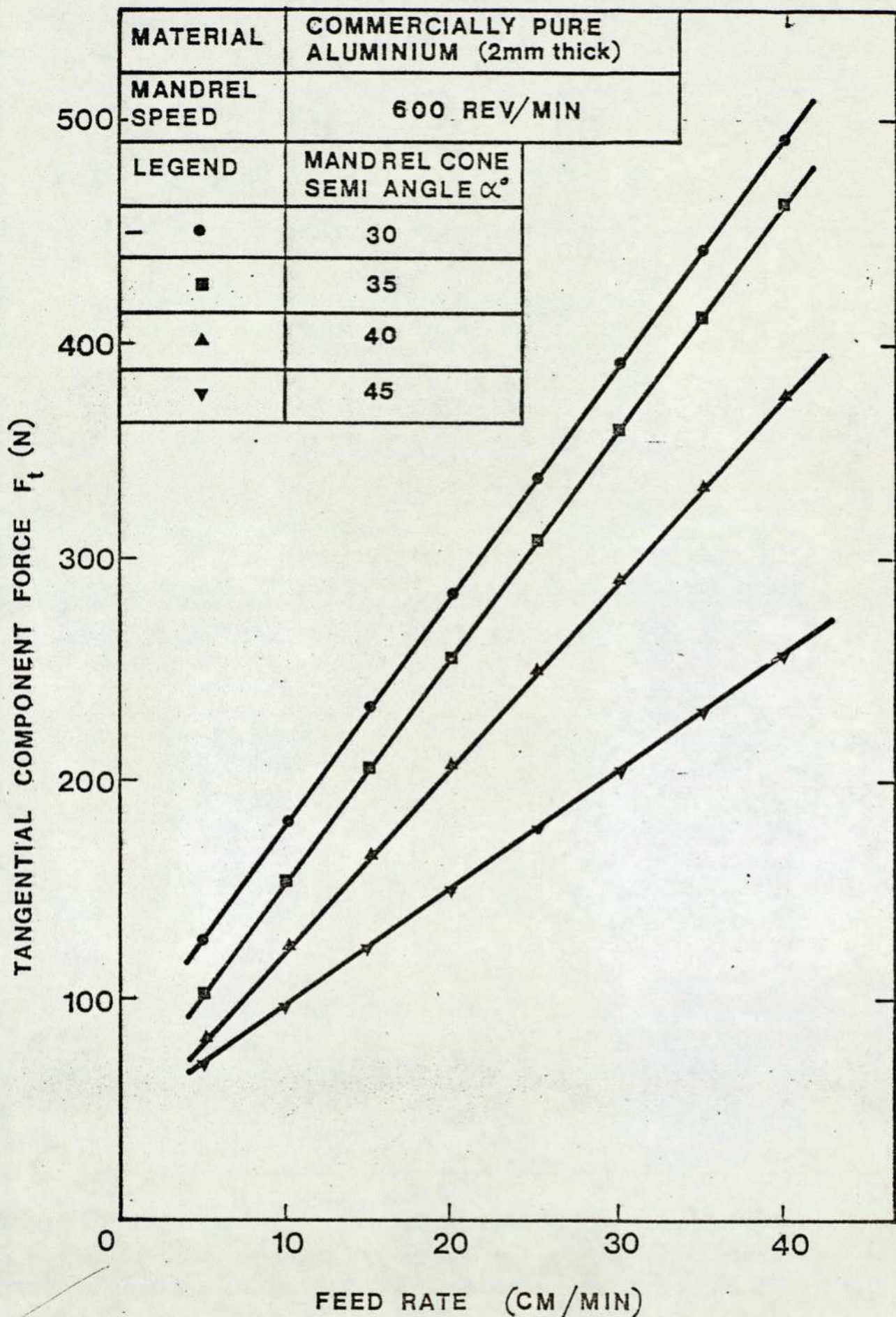


FIGURE 30 Relationship between tangential component force and feed rate for various cone semi-angles for commercially pure aluminium

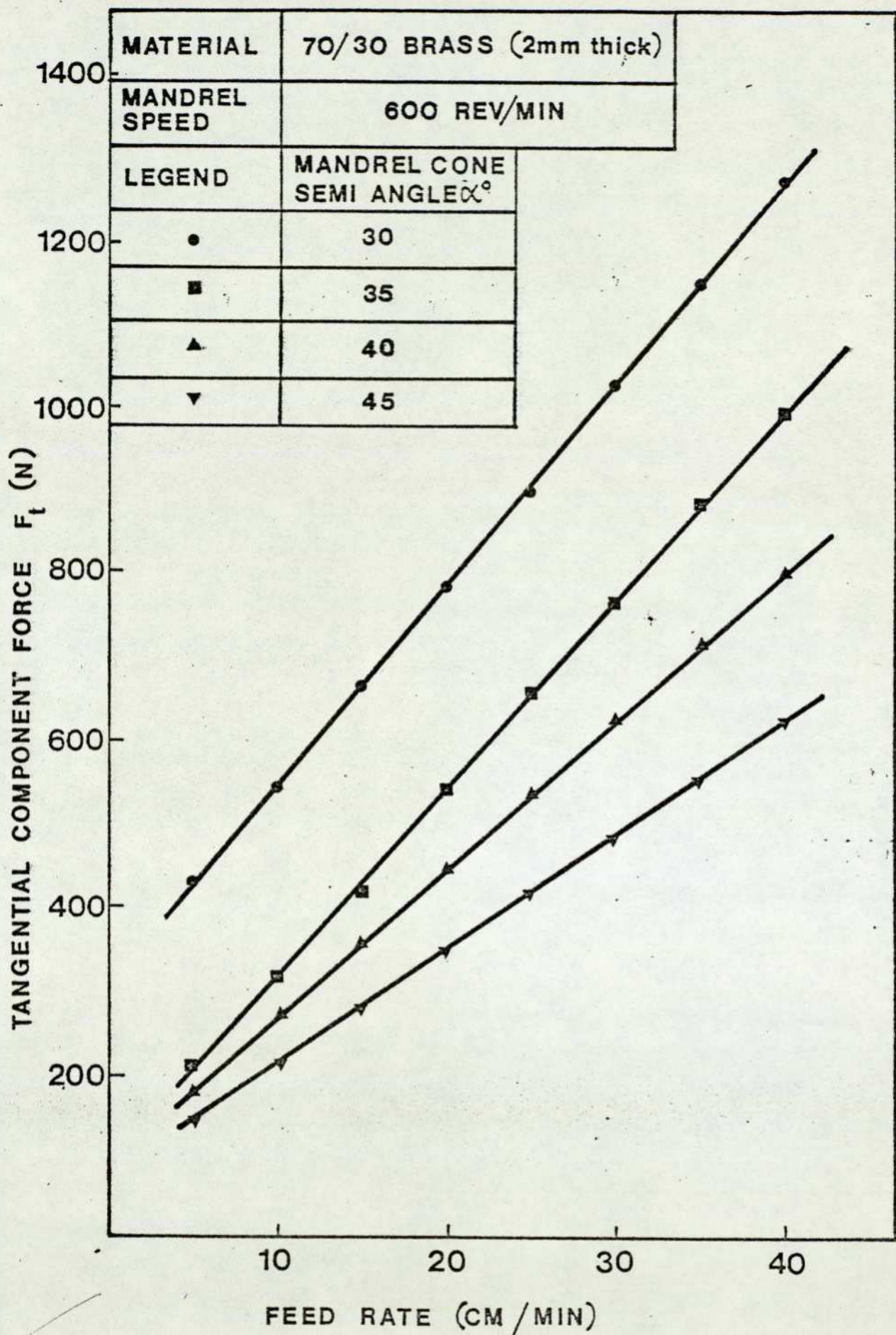


FIGURE 31 Relationship between tangential component force and feed rate for various cone semi angles for 70/30 brass

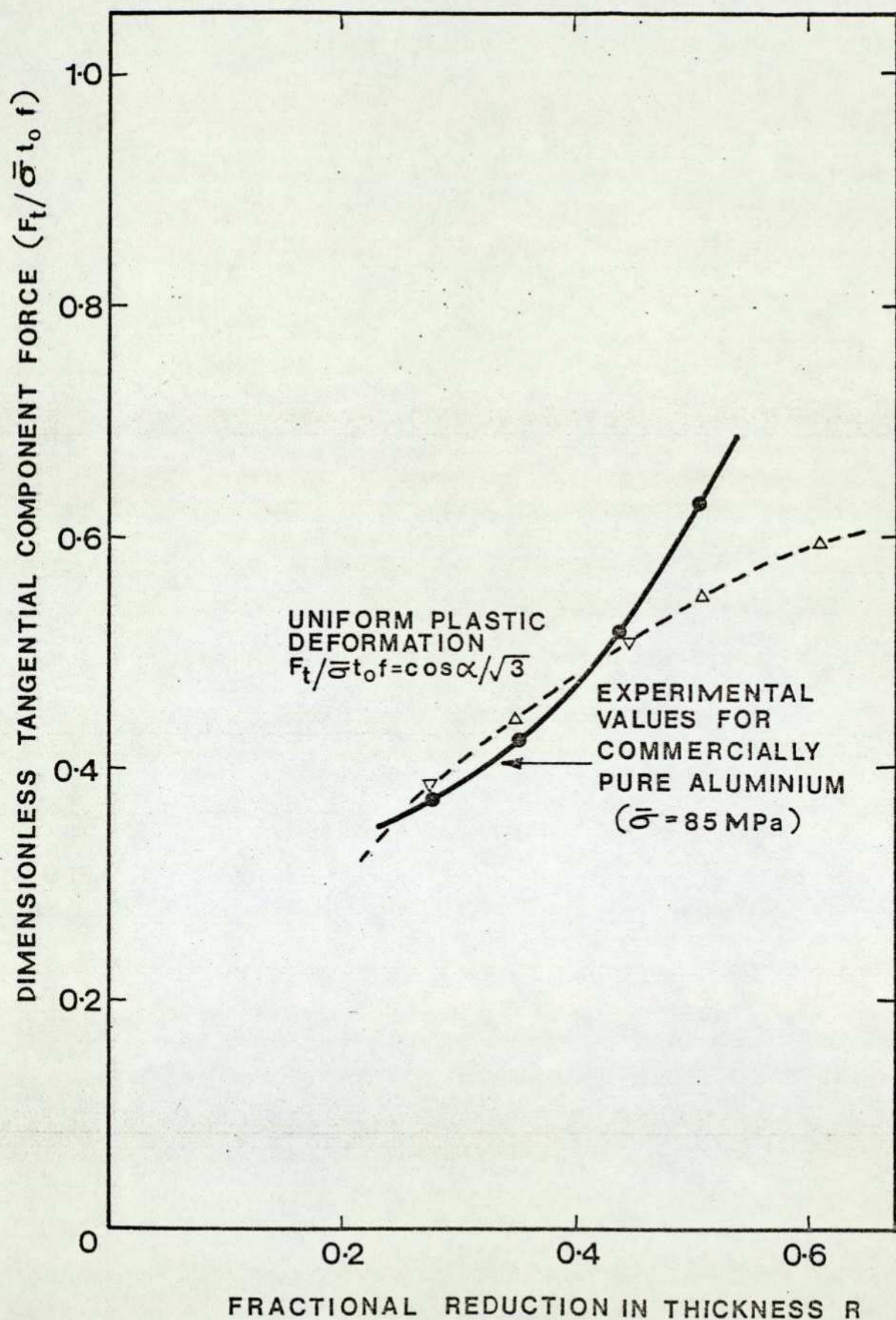


FIGURE 32 Relationship between dimensionless tangential component force predicted by uniform deformation method and the experimental values for commercially pure aluminium

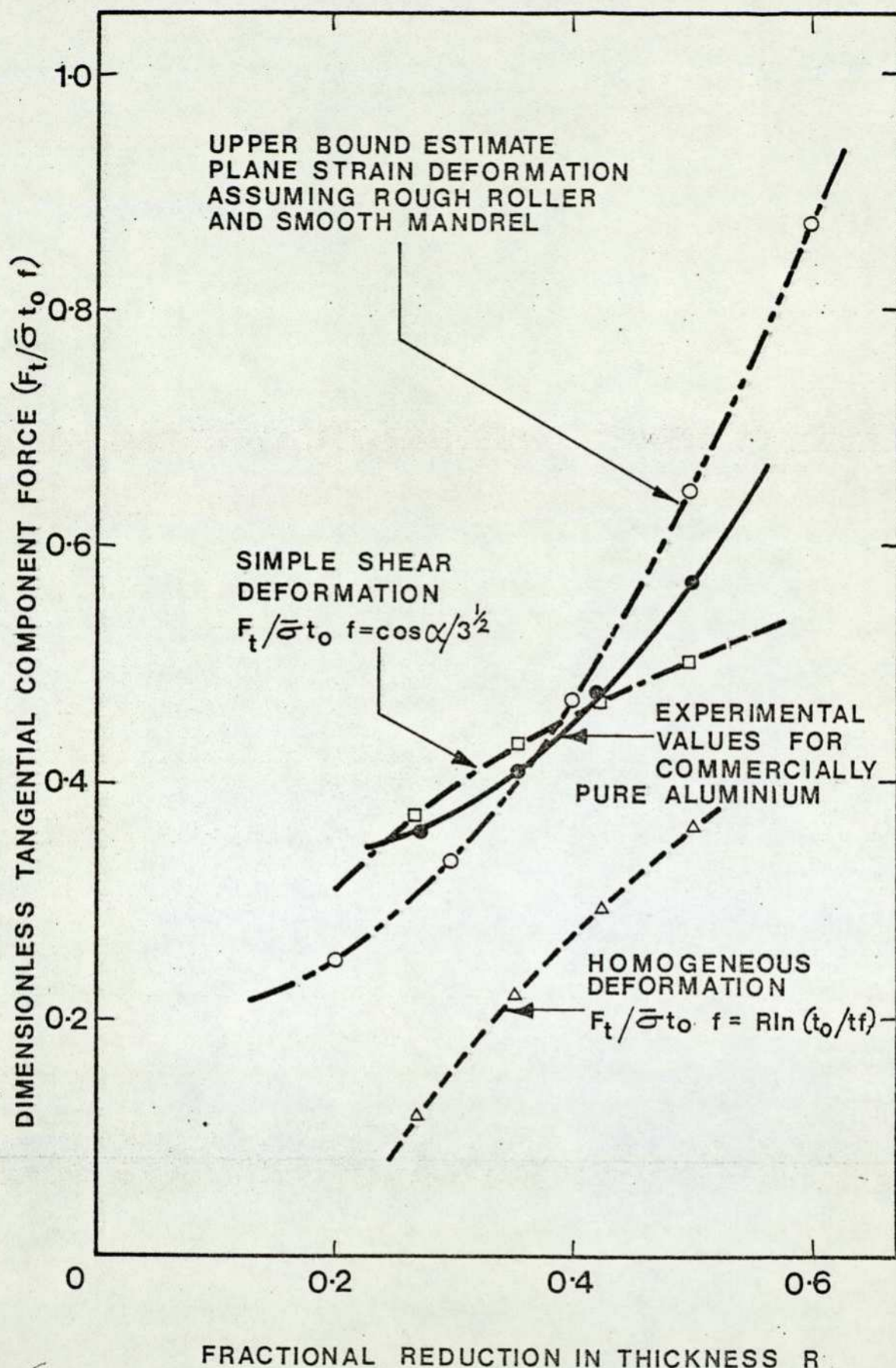


FIGURE 33 Comparison of the experimental values of dimensionless tangential component force with the elementary theories and an upper bound estimate assuming plane strain deformation

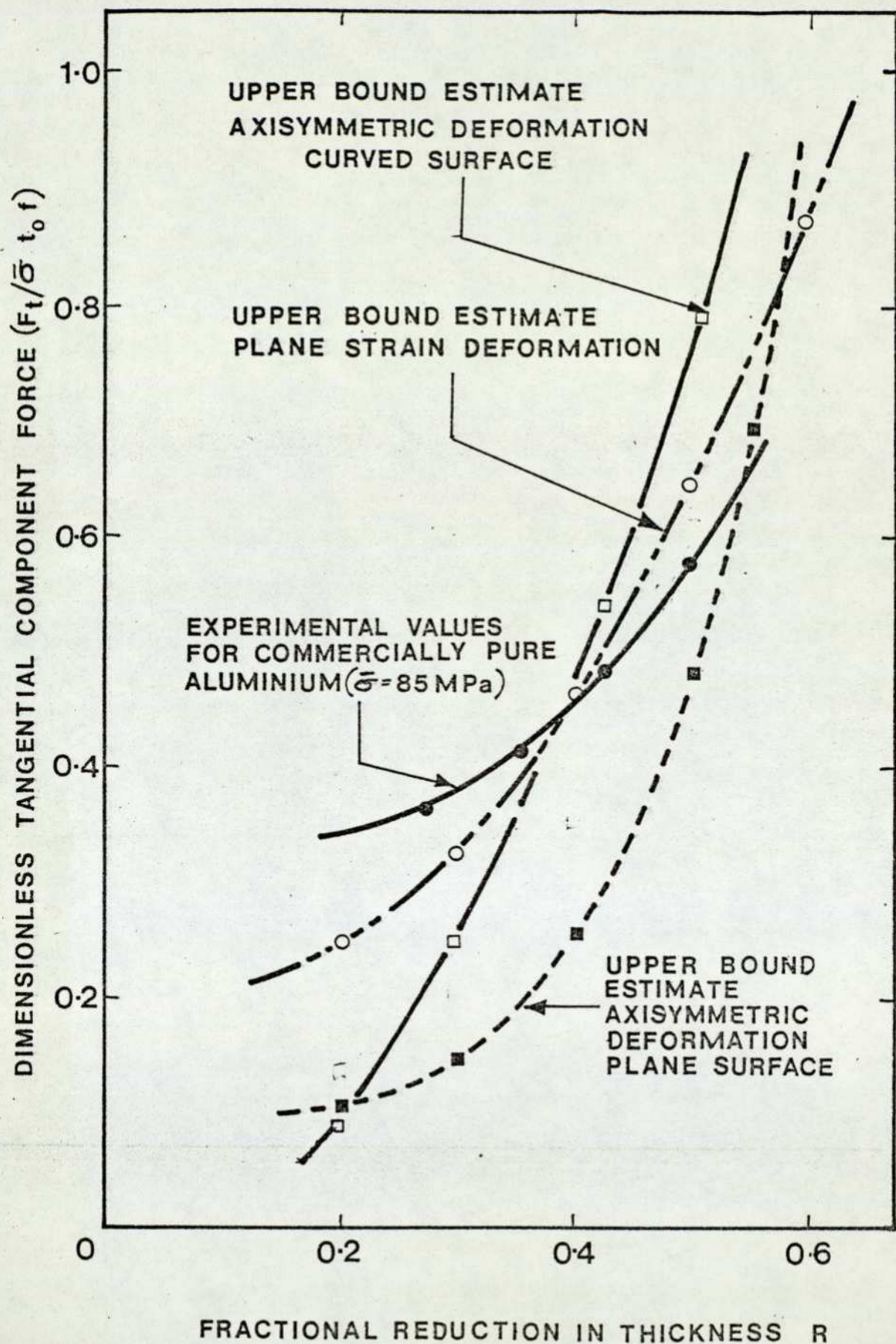


FIGURE 34 Comparison of various upper bound estimates and experimental values for commercially pure aluminium

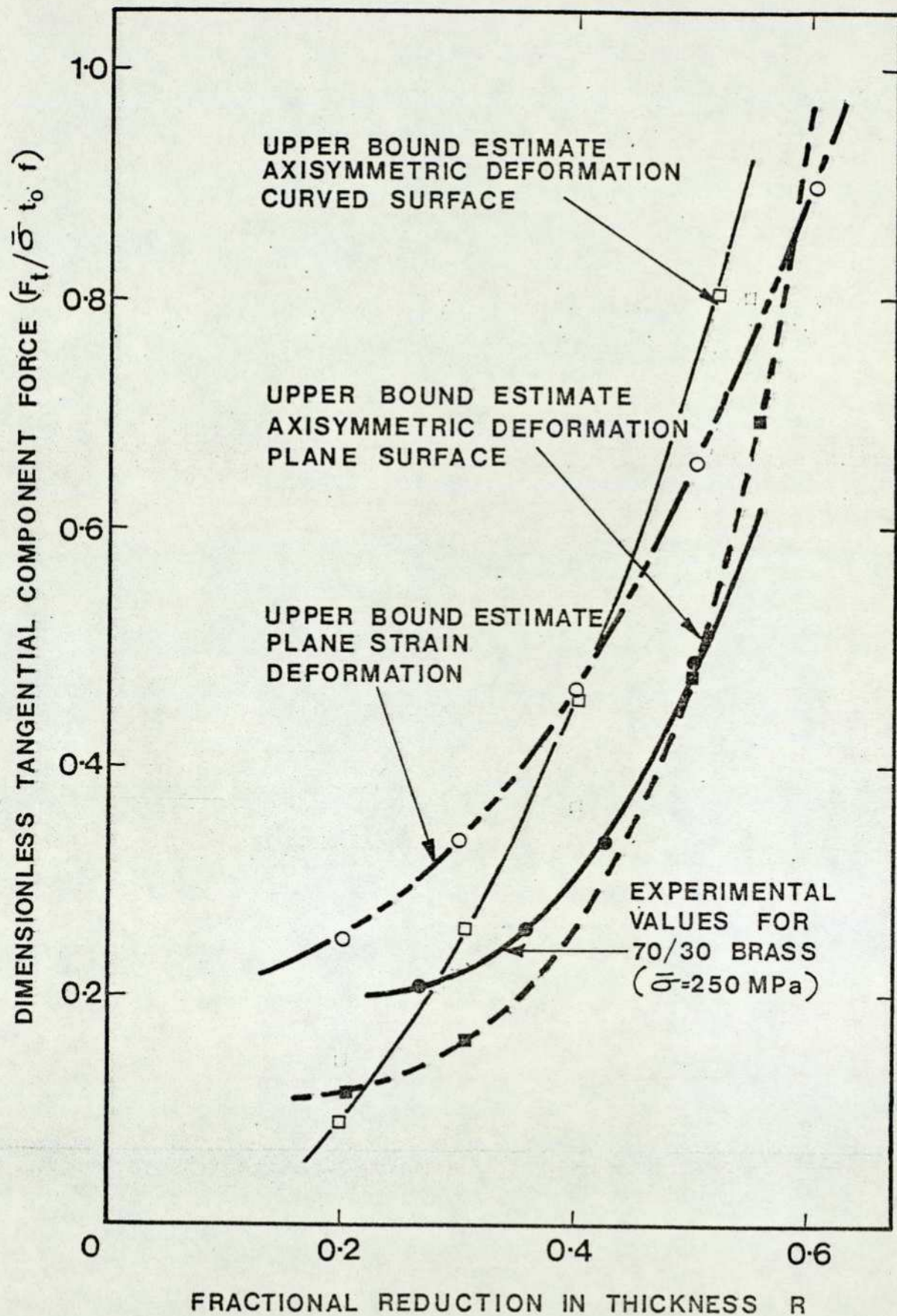


FIGURE 35 Comparison of various upper-bound estimates and experimental values for 70/30 brass

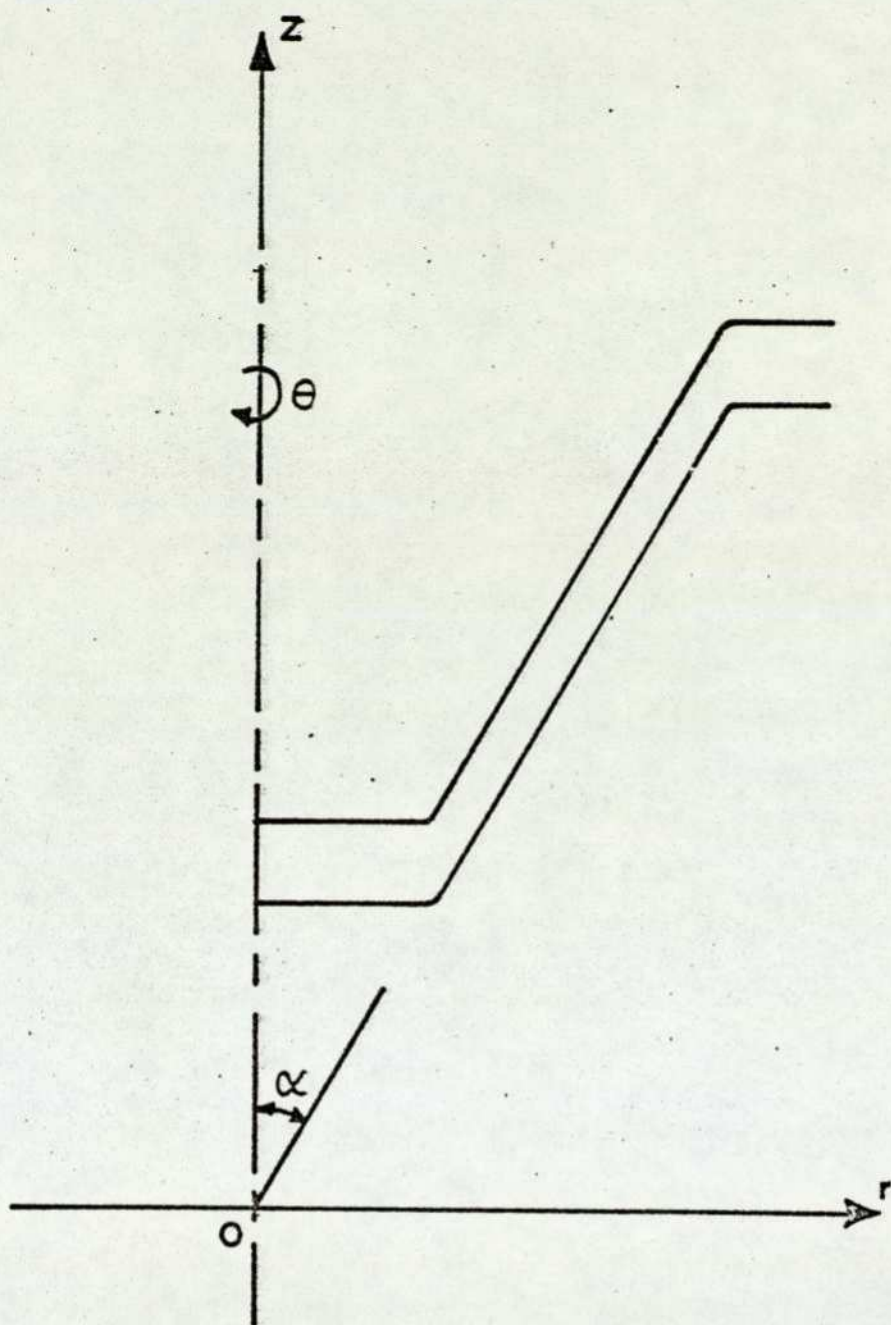


FIGURE A-1 Cylindrical co-ordinate system

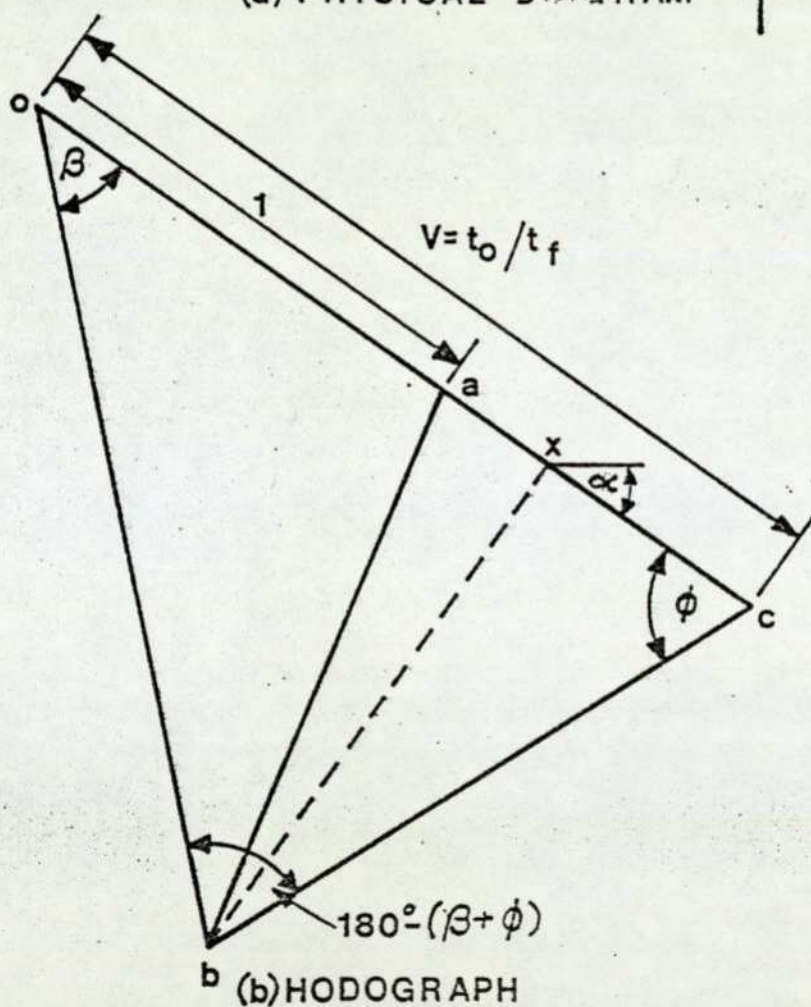
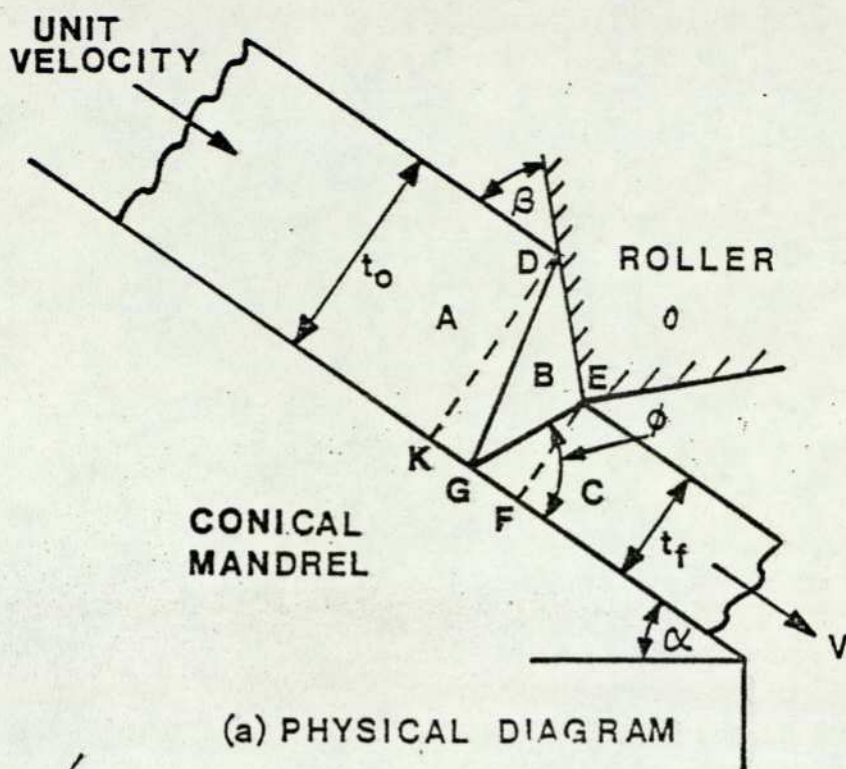


FIGURE A-2 Spin-forging of a sheet metal cone assuming plane strain deformation
a-Physical diagram comprising a single triangular field of tangential velocity discontinuities
b-The corresponding hodograph

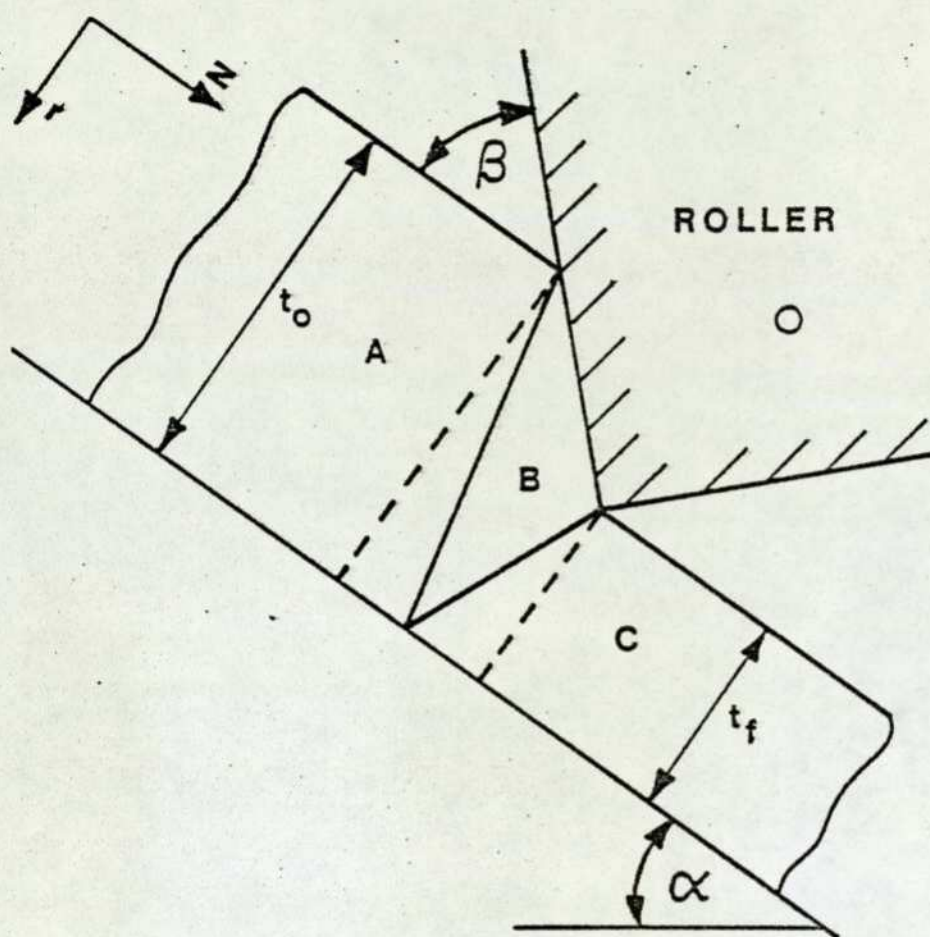
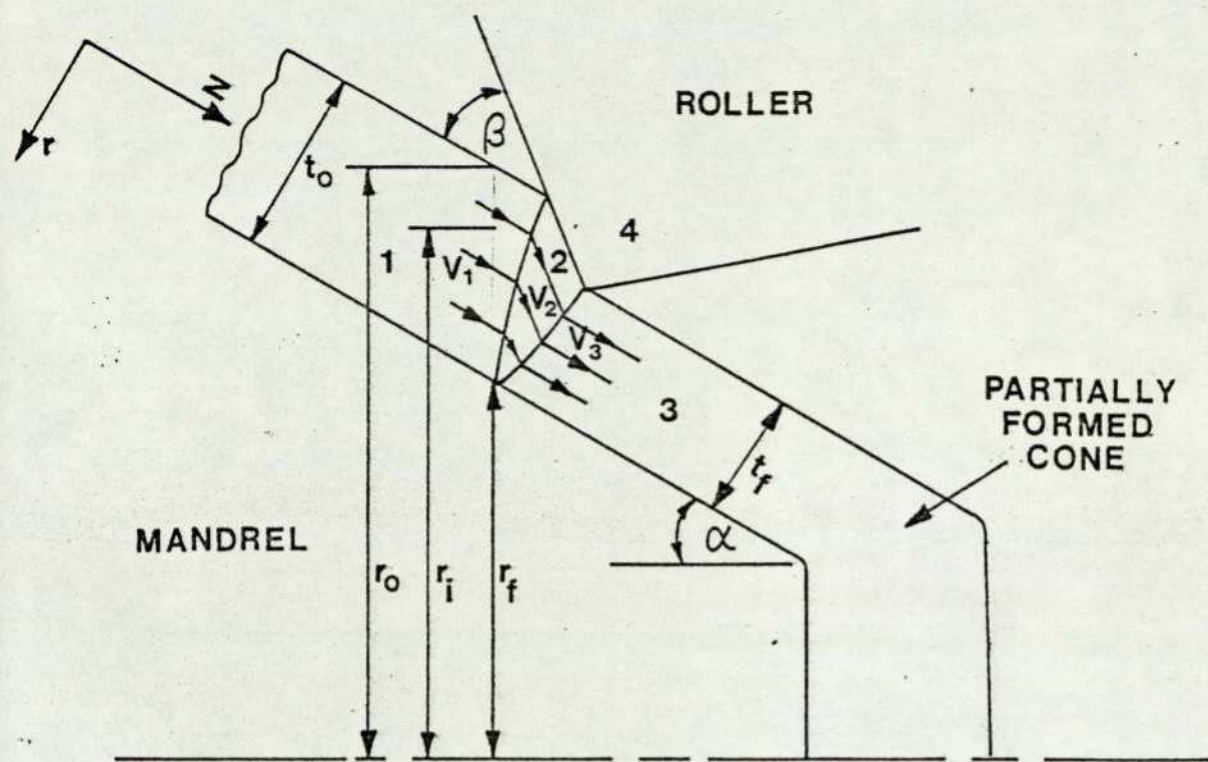
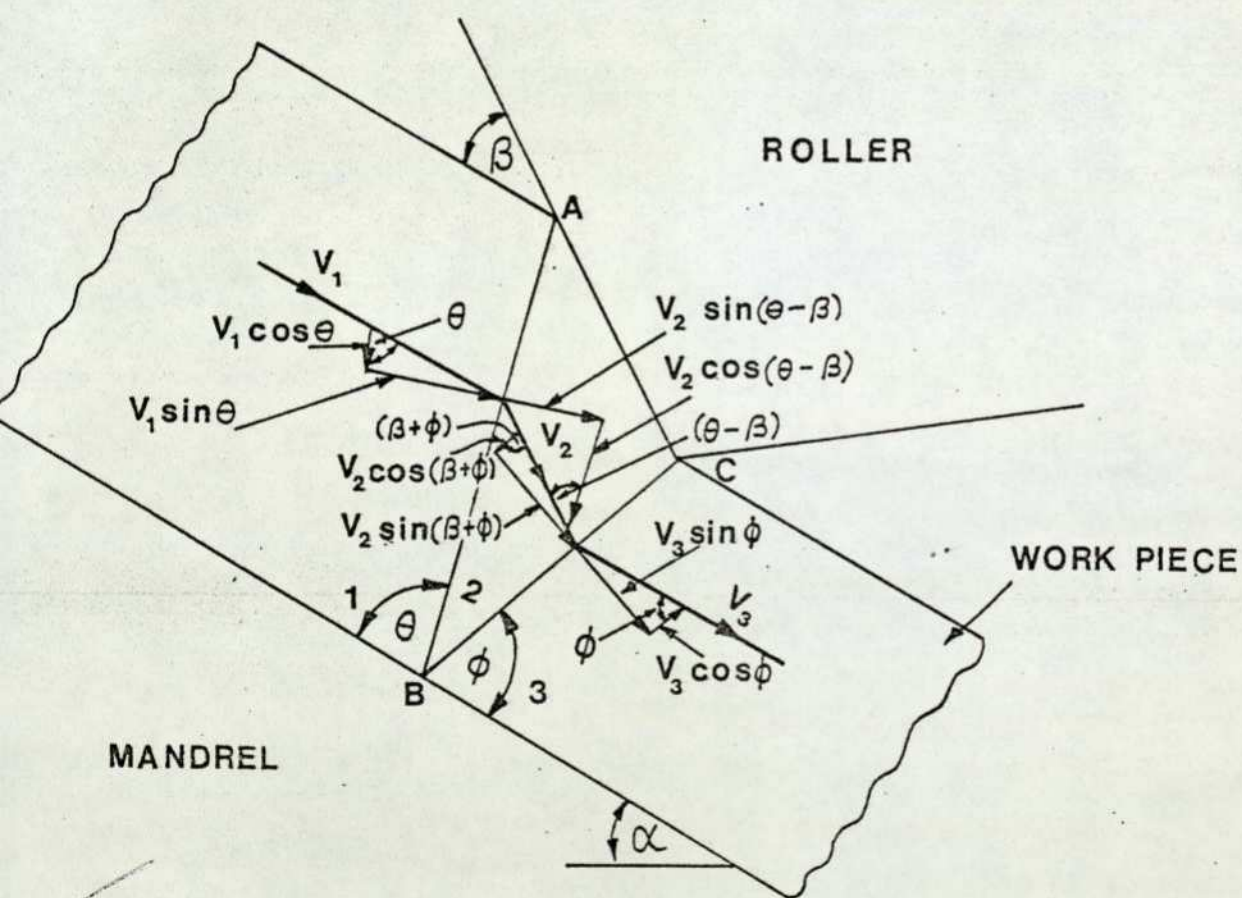


FIGURE A-3 Physical diagram for upper bound estimate assuming axisymmetric deformation with straight lines of tangential velocity discontinuity on a median plane



(a) ASSUMED MODE of DEFORMATION



(b) PROPOSED VELOCITY FIELD

FIGURE A: 4 Assumed mode of deformation and proposed velocity field for upper bound estimate assuming axisymmetric deformation and curved surfaces of admissible velocity field