

COMPLEX & INCREMENTAL STRESS CREEP
OF A HIGH STRENGTH ALUMINIUM ALLOY
AT ELEVATED TEMPERATURES

(ALLOY: HIDUMINIUM RR58 SPECIFICATION DTD 731)

by

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ABSTRACT

A theory for creep rates under complex and incremental stresses is deduced from experimental data concerning complex creep at elevated temperatures for the test material HIDUMINIUM RR 58 - Specification DTD 731. The most important results are for tubular specimens tested at 150°C and 250°C under incremental loads. The analysis of results relates to steady state creep only.

Modified relationships in stress equivalence and strain equivalence are proposed to account for thermal softening, polygonization, recrystallization and the resulting exaggerated flow in the direction of the applied shear. (The original equations are based on the hypothesis of Von Mises).

A further relationship is suggested between the immediate total energy of distortion and the subsequent creep work rate.

Results of the static tests and the results of the tests for creep behaviour under complex loading are presented and compared with the results of static torsion and simple incremental torsion creep tests on the basis of the proposed equations.

An appendix describes the complex creep testing machine, furnace, extensometers and ancillary equipment which were designed for this programme and later modified to suit the behaviour of the test material.

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NOMENCLATURE

l_0, l	Original and final lengths of test specimen
t	Time
σ	Direct stresses
ϵ	Engineer's direct strain
γ	Engineer's shear strain
$\sigma_x, \sigma_y, \sigma_z$	Suffixes x, y and z define directions in terms of
$\epsilon_x, \epsilon_y, \epsilon_z$	Cartesian coordinates
$\dot{\epsilon}_c$	Direct secondary creep rate
T	Absolute test temperature
T_0	Absolute equicohesive temperature
$\epsilon_{ij}, \dot{\epsilon}_{ij}$	Strain and strain rate tensors
σ_{ij}	Stress tensor
J_2	Second invariant of the stress equation
$\sigma_1, \sigma_2, \sigma_3$	Principal stresses
$\epsilon_1, \epsilon_2, \epsilon_3$	Principal strains
$\dot{\epsilon}_1, \dot{\epsilon}_2, \dot{\epsilon}_3$	Principal strain rates
$\tau_{xy}, \tau_{yz}, \tau_{zx}$	Shear stresses in the planes defined by the Cartesian coordinates
$\dot{\epsilon}_x, \dot{\epsilon}_y, \dot{\epsilon}_z$	Direct strain rates in the direction of the Cartesian coordinates
$\dot{\gamma}_{xy}, \dot{\gamma}_{yz}, \dot{\gamma}_{zx}$	Shear strain rates in the planes defined by the Cartesian coordinates
ϵ_{ij}^p	Plastic strain tensor
$\epsilon_e, \dot{\epsilon}_e$	Equivalent strain and equivalent strain rate
W_p	Work expended during plastic and creep strains

NOMENCLATURE (contd)

U_{SH}	Elastic shear strain energy	
σ_{e_i}	Current equivalent direct stress	) suffix 'i' defines the load stage
$\dot{\epsilon}_{e_i}$	Current equivalent direct strain rate	

All other symbols are defined where used.

INTRODUCTION

Up to the turn of the century most engineering components tended to be large because the analysis for estimating nominal dimensions was approximate. Moreover, generous allowances were made for margins of error. In recent years, however, with more accurate analyses and improvements in the mechanical properties of engineering materials it has been possible to reduce the overall size of components. Amongst the benefits derived from this reduction are smaller inertia and lower material cost, but unfortunately the operating stresses increase. The high stresses produce marked fatigue and creep effects.

In post-war years the phenomenon of creep has become increasingly important. The modern use of materials at elevated temperatures has accelerated and magnified creep strains. These are so significant, in particular applications, that they have become major design considerations. High stresses are commonly encountered in high pressure pipes, high pressure vessels, turbine discs, turbine blades, aircraft components and many other major installations and structures.

In the past three decades creep behaviour has been investigated by two distinct groups of workers with certain detailed areas of interest.

(1) Metallurgists, who are primarily interested in microstructural behaviour with particular reference to grain and atomic structure energies and displacements.

(2) Technologists and engineers, who observe the phenomenon as a macrobehaviour. They are generally interested in establishing rules of thumb as well as more sophisticated mathematical analyses which can be suitably employed as design criteria. (This thesis is presented from the viewpoint of the technologists but findings of the metallurgists have been considered).

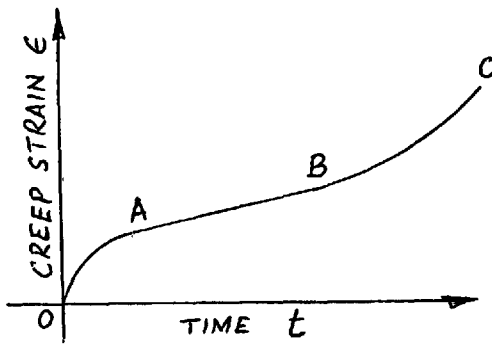
Early investigators tried to establish a law for creep behaviour by a critical study of the parameters involved. Ultimately a mathematical relationship was established by Andrade¹ who proposed an equation for tensile creep in the algebraic form:

$$l = l_0 (1 + \beta t^{1/3}) e^{Kt} \quad (1)$$

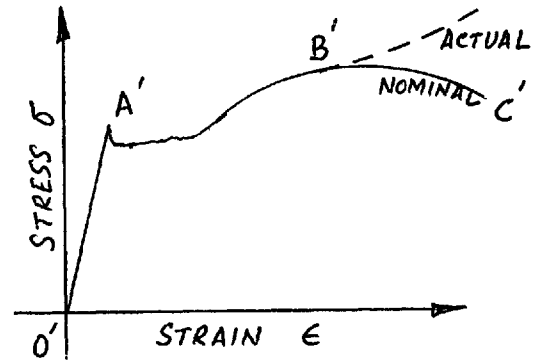
Where β and K are constants for the material at the given temperature.

Various theories of elastic failure have been postulated and these have arisen from inconsistent behaviour between predicted and actual results. In general, static tensile data were the yardstick and therefore the corresponding tensile creep data have formed the basis for design.

At this point it is worth considering the similarity between static and time - dependent strain behaviours under load, as shown in Figs. 1 and 2. The



Constant stress - constant temperature creep Fig. 1



Static Tension Fig. 2

three phases of creep (primary OA, secondary AB and tertiary BC) are comparable with the elastic recoverable phase O'A', the controlled plastic phase A'B' and the phase B'C' respectively in static tension. B'C' is the uncontrolled plastic phase which leads to neck formation and ultimate fracture. The static elastic strain O'A' compares favourably with the primary creep strain OA where unloading of the specimen restores the original physical geometry. Strain A'B' in the controlled plastic deformation is similar to the limiting secondary creep strain AB

that can be accepted within the designed working life of a component. The uncontrolled plastic deformation and fracture B'C' is akin to the tertiary creep and rupture strain BC.

The strain behaviour of materials under load is a time-dependent or time-independent plastic flow phenomenon. Hence the theories based on time independent flow hold for time dependent conditions as well. The creep phenomenon is a stress dependent flow in solid solution state. If creep occurs, work must be absorbed, therefore any hypothesis formulated for creep behaviour must be based on an energy equation. In constant temperature and constant stress tensile creep too many variables have deliberately been ignored. The basis for design determined on such data is likely to be erroneous and therefore it is important to investigate behaviour under a complex stress pattern.

Biaxial loading in creep tests has been extensively used in recent years to obtain complex stress states. A vast amount of work on a number of materials has been carried out in this field at the National Engineering Laboratories by the late Dr. A. E. Johnson and his co-workers.

Whereas it has been largely appreciated that a multiaxial load system will produce a comprehensive basis for analysis, very little work on creep under varying and complex stresses has been done. The current research programme was undertaken to investigate this area. It was proposed that tubular test specimens be loaded under combined tension-torsion to simulate a multiaxial stress state. Creep strains being time dependent, each load stage was allowed to exist for a finite period before it was altered to introduce a new stress stage. Each specimen was therefore loaded in the combined load state defined as above. These stages were altered at regular intervals to vary the multiaxial stress level. The tests are referred to in the thesis as INCREMENTAL and COMPLEX stress creep tests.

The report deals with incremental stress secondary creep behaviour of the alloy H18DUMINIUM RR 58 at elevated temperatures. This alloy is of particular interest owing to its use in high speed aircraft in which increasing speeds have led to stress and temperature variations.

No accurate and reliable static data, simple creep or incremental stress creep data existed for the test material. A considerable number of tests were conducted. Equations of equivalence based on the criterion of Von Mises were invalid and had to be modified. In spite of this modification the correlation between incremental creep tests for tension and torsion separately showed scatter which could not be explained. The correlation proposed between the energy of distortion and the subsequent creep work rate gives a greater compatibility.

LITERATURE SURVEY

The need for developing a creep behaviour relationship was mentioned in the previous chapter. In order to develop coherent relationships for such a behaviour under varying and complex stress conditions at elevated temperatures, it is necessary to review any relevant recorded work available.

Considerable theoretical and experimental work has been conducted in simple tensile creep since the early 1920's, followed by research into single stage complex stress creep. Subsequently, elevated temperature creep under complex stresses was undertaken at the National Engineering Laboratories by Johnson² and his co-workers. The work for this thesis was conducted at much higher stress levels and has therefore exhibited a different pattern of behaviour.

Simple Creep Theory:

Simple tensile creep was originally characterised by Andrade¹ and was related to high stresses independent of the prevailing temperatures. Dickerson³ proved that high temperatures accelerate the process of creep and therefore induce it at low stress levels as well. Since the 1930s, the creep phenomenon has become an essential point of consideration in design because of increasing operating temperatures.

Owing to the development of more precise strain measuring techniques the equation of Andrade has in part been superseded by a more theoretical relationship, defined as 'logarithmic creep'.

$$\epsilon_c = \epsilon_0 \log_e (at+1) \quad (2)$$

where $\epsilon_0 = \text{Constant}$

$a = \text{Constant}$

The equation in (2) did not define the full creep curve and therefore a comprehensive form has been proposed in the form of a polynomial. This polynomial is expressed as:-

$$\epsilon_c = \epsilon_0 + \sum a_i t^{m_i} + \sum b_j t^{n_j} \quad (3)$$

where $i = 1$ and $0 < m_i \leq 1$
 $j = 1$ and $n_j > 1$

a_i and b_j are functions of temperature and stress for the particular material and therefore the polynomial fits the experimental creep curves more accurately.

Creep rates, depend on the mechanical, metallurgical and thermodynamic parameters, namely, stress, strain and temperature. Johnson and Mcvetty⁴ have therefore proposed

$$\dot{\epsilon}_c = A \sinh \frac{\sigma}{\sigma_0} \quad (4) \text{ for secondary creep}$$

where σ = current stress

and A and σ_0 are material constants dependent on the test temperature.

Most of the experimental workers have favoured current stress as the dominant parameter and have used the empirical relationship for secondary creep rate:-

$$\dot{\epsilon}_c = A_1 \sigma^n \quad (5)$$

A_1 and n are constants for the material at the temperature used.

Engineers' understanding of the creep phenomenon as a macroscopic behaviour of material is made complicated when high operating temperatures are used and the microbehaviour of most alloys can no longer be ignored. The inter-crystalline flow that follows has a considerable influence. Dorn⁵ relates the creep rate to a time-activation energy parameter for temperatures above 0.6 of the absolute melting temperature. He has, however, demonstrated that if the process of the diffusion in the metal occurs, the activation energy only varies by 5 to 10

per cent. Therefore he proposed a temperature-independent function of stress for the secondary creep rate in the form:-

$$\begin{aligned}\dot{\epsilon}_c &\propto \sigma^n && \text{similar to equation (5) - for low stress levels} \\ \dot{\epsilon}_c &\propto e^{B\sigma} && \text{for high stress levels - (6)}\end{aligned}$$

where n and B are constants

Weertman⁶ and Shahinian in their work relating to polycrystalline nickel have proposed an expression which simplifies to a similar expression:

$$\begin{aligned}\dot{\epsilon}_c &= A e^{[\Delta H_D + f(\sigma)]/RT} \\ &= A e^{\Delta H_D} e^{-RT} e^{f(\sigma)}\end{aligned}$$

where ΔH_D approximates to the energy of self diffusion as long as precipitation and ageing reactions are not prominent.

R = gas constant

T = absolute test temperature

$f(\sigma)$ = a function of applied stress

At constant temperature this relationship simplifies to the form of equation (6).

Metallurgical Aspects

Behaviour of Polycrystalline Materials:

In polycrystalline materials most of the slip takes place at the grain boundaries. At elevated temperatures the grain boundary strength reduces more rapidly than the grains themselves as shown in Fig. 3. Cottrell⁷ states that the limiting temperature, T_0 , at which the two strengths are identical is approximately 0.6 T_m .

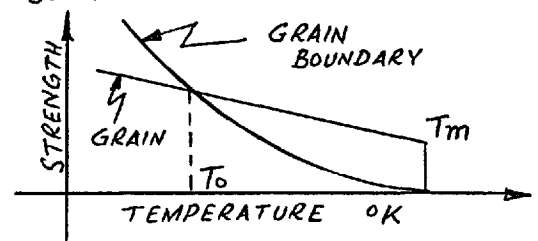


Fig. 3.

$$\text{i.e. } T_0 = 0.6 T_m \quad - (8)$$

where T_m ($^{\circ}\text{K}$) = Absolute melting temperature.

T_0 is defined as equicohesive temperature. It is, however, commonly suggested that the value of T_0 may be as low as $0.45 T_m$. From the value in equation (8) it can be stated that creep strains at temperature higher than T_0 are caused by grain boundary slip and sub-grain formation. Cottrell⁷ further suggests that the shear strain rate

$$\dot{\gamma} = A e^{V(\sigma_0 - \sigma)/K T} \quad - (9)$$

where K = Boltzman constant

T = Absolute Test temperature

σ = applied stress

and A , V , σ_0 and n are constant parametric values for the particular material.

He has, however, pointed out that in the simplest cases $n = 1$ and V = 'characteristic volume' which measures the size of a single creep event.

Mullendore⁸ and Grant have investigated the grain boundary breakdown. They have shown that grain boundary serrations develop during creep. Their work on aluminium-magnesium alloy in the solid solution state has shown that, under creep conditions of stress and temperature, grain boundary sliding and migration occur. The boundaries become roughened to a serrated form which possesses a striking regularity. This is closely related to ^{ter}incrySTALLINE voids. At very high stresses, these serrations are very fine.

Bogardus⁹ and co-workers considered the microstructure after strain. Electron microscope examination after creep reveals polygonization and decrease in dislocation density which are caused by high temperatures. It was observed that as these effects lead to loss of strength, subsequent boundary slip results in incipient cracking along very fine slip bands. Any further strain is due to crack propagation. Cottrell¹⁰ postulates the same process of reduced strength and increasing creep strain. This is also confirmed by the work of Hanson¹¹ and Wheeler. These findings could explain the behaviour of the tubular specimens at the high temperature in the present

test programme. Investigation of such an occurrence is not a part of this work.

Effect of Solute Atoms

In a material, if the solid solution state is achieved at high temperatures, ageing will occur at working temperatures. If there is prior ageing, subsequent work hardening can be avoided. This will allow the slip bands to grow.

Materials which age with strain are susceptible to brittle fracture at low temperatures. Furthermore, if strain rates are high the solute atoms have little chance to migrate to the dislocations and hence a brittle fracture is probable. This type of phenomenon explains reduced creep strain to fracture at the lower test temperature of 150°C.

For steady state creep behaviour the facts given above suggest that at each temperature there exists a limiting stress below which the creep rates will be markedly reduced, i.e. fewer slip bands will occur. (On a macroscopic scale for the test material, it is reasonable to conclude that a degree of compatibility exists between the total energy of distortion - elastic and plastic - and the subsequent creep work rate.

This is discussed later in the section headed Mathematical analysis).

Effect of Excessive Temperature at High Stress Levels

In copper at high temperature, the activation energy ΔH for creep rises with temperature. This was shown by Dorn in his paper 'Spectrum of activation energy creep' given at the ASTM symposium on creep and recovery at Cleveland in 1957.

The activation energy is a limiting value which when exceeded must disrupt the progressional equilibrium. In aluminium with lesser extended dislocations the rise in ΔH is slower. However, such a behaviour of activation energy is bound to produce a creep law that is temperature-dependent.

Weertman¹² suggests that creep based on climbing dislocation at high temperatures, where slip phenomenon occurs by virtue of jog climb and the creation of new slip planes, will observe the law for steady state creep as

$$\dot{\epsilon}_c = \frac{N_c A D}{h} \quad - (10)$$

- where N_c = Dislocation per unit volume
 A = glide area per dislocation in new slip plane after climbing
 K = Boltzmann's constant
 D = Self-diffusion coefficient = $D_0 e^{-\Delta H_D / RT}$
 H = activation energy for self diffusion
 D_0 = a constant
 b = Burgers' vector
 h = height which must be climbed for 'escape' to a new slip plane
 σ_i = Internal stress

In practice this expression gives a good prediction for steady state creep rate, but at low stress levels when $\sigma_i b^3 / K T < 1$, the temperature effect is negligible and therefore the power law stated in equation (5) is quite accurate.

This strengthens the argument that a transition stress exists in elevated temperature creep where the behaviour deviates from that predicted by the power law in (5). Beyond the transition stress, behaviour has been proposed to follow the law in equation (6).

Effect of Precipitation on Strength

The strength of many alloys depends upon the precipitation of the hardened secondary phase alloys within the material matrix. In aluminium, copper forms the intermetallic compound CuAl_2 at reasonably low temperatures as shown in Fig. 4.

The test material for the present work is HIDUMINIUM RR58 which is primarily an aluminium - copper alloy. It is solution-treated at 530°C and quenched in boiling water so that the precipitates will be formed at the grain boundaries as shown in this binary diagram at A. When it is aged at 200°C for 20 hours (point B) the precipitate of CuAl_2

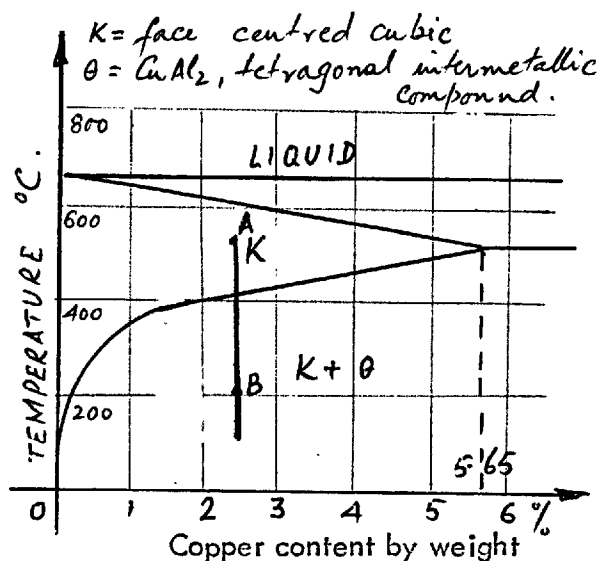


Fig. 4

disperses throughout the matrix of K . Longer ageing gives greater stability and high creep strength up to a limiting value only. Any further prolonged ageing will lead to deterioration in the mechanical properties.

Effect of Precipitation During Creep

At low temperatures the material matrix is strengthened by the initial precipitation and therefore the creep rates are low. When further precipitation occurs the matrix is starved of the hardening solute atoms. This produces thermal softening and aggravated creep rates.

These facts above lead to two hypotheses as stated by Dorn¹³ and Cottrell¹⁴. Firstly, that the initial precipitation raises elastic strength and helps develop creep resistance. Secondly, any intense ageing helps to coagulate the precipitates and effectively starves the matrix of solute atoms, thereby producing thermal softening.

This explains why, after a limiting soak period, particularly of 48 hours at 250°C in the present test programme, high creep rates were encountered.

Crack Initiation and Rupture

Greenwood and Miller¹⁵ have shown that voids, developing at grain boundaries during creep are a mode of local stress relief. When a local plastic slip in the grains is forced by adjacent grain boundary movement, a fold formation ensues. If local constraints occur in thin-walled closed sections, the voids in their advance through the thickness, at rapid rates, initiate cracks.

In the present tests, this phenomenon explains the collapse of tubular specimens, which occurs soon after the onset of the tertiary phase. The crack propagation results in an open section as opposed to the original closed section. Hence the fold formation, owing to the void propagation, must run normal to the direction of the applied torque. This is recognised as shear flow in the warping of thin closed sections  due to external torque. The one inch gauge length specimens for simple tensile creep do not exhibit such an effect. If the voids propagated normal to the loading axis brittle fracture will be observed. If these voids run axially as shown in fig. 57 the strength and creep resistance remains unimpaired. Therefore considerable scatter in test results will appear.

Mathematical Theory of Steady State Creep under Complex Stresses

Introduction

The creep phenomenon has been expressed in the preceding section as an energy activated and time-dependent plastic flow. It is proposed to explore the various popular mathematical relationships and to suggest the final form for the creep behaviour. It has to be consistent with the experimental data obtained in the present tests for the material H14Mn2Si alloy at elevated temperatures.

The steady state creep rate has been commonly expressed by most experimentalists, e.g. Mcvetty⁴, Johnson² and Dom¹⁶, etc. as:

$$\dot{\epsilon}_c = A_0 \sinh \frac{\sigma}{\sigma_0} \quad - (4)$$

$$\dot{\epsilon}_c = A_1 \sigma^n \quad - (5)$$

$$\dot{\epsilon}_c = A e^{B\sigma} \quad - (11)$$

where A_0 , A , A_1 , B , σ_0 and n are constant parameters dependent on temperature, material and stress level. The closest fit observed for the simple tensile creep data and simple torsion creep data obtained in the present tests when equation (11) has been used, has proved it to be the valid empirical relationship. To establish this correlation as unique, various stress systems have to be investigated, in particular the multiaxial stress system.

Complex Stress Equivalence

The equations mentioned above represent secondary creep rate for constant temperature. The uniqueness of stress can only be defined by the actual stress and the corresponding strain rate. A very comprehensive review is given by Johnson² in the series of the Metallurgical Reviews published by the Institute of Metals. He expresses creep rate as a function of the generalised stress tensors. In this form the secondary creep rate is expressed as:

$$\dot{\epsilon}_{ij} = \frac{d \epsilon_{ij}}{dt} = F(J_2) (\sigma_{ij} - \delta_{ij} \sigma) \phi t \quad - (12)$$

where $J_2 = \frac{1}{6} \sum (\sigma_i - \sigma_j)^2$, $\phi t = m t^{m-1}$

$$\sigma = \text{Hydrostatic stress} = \frac{\sigma_1 + \sigma_2 + \sigma_3}{3}$$

i and j have values 1, 2 and 3 corresponding to the coordinate axes.

When equation (12) is expressed in terms of principal stresses, it is transformed into the following principal strain rates as:-

$$\dot{\epsilon}_1 = f[\Sigma(\sigma_1 - \sigma_2)^2] [(\sigma_1 - \sigma_2) - (\sigma_3 - \sigma_1)] m t^{m-1} \quad - (13)$$

$$\dot{\epsilon}_2 = f[\Sigma(\sigma_1 - \sigma_2)^2] [(\sigma_2 - \sigma_3) - (\sigma_1 - \sigma_2)] m t^{m-1} \quad - (14)$$

$$\dot{\epsilon}_3 = f[\Sigma(\sigma_1 - \sigma_2)^2] [(\sigma_3 - \sigma_1) - (\sigma_2 - \sigma_3)] m t^{m-1} \quad - (15)$$

In secondary state the function 'f' is determined with reference to tensile creep data as a simple function, independent of time as:-

$$\dot{\epsilon}_e = f(\sigma_e) \quad - (16)$$

Hence the equation above becomes:-

$$\dot{\epsilon}_1 = \frac{\dot{\epsilon}_e}{\sigma_e} \left[\frac{(\sigma_1 - \sigma_2) - (\sigma_3 - \sigma_1)}{2} \right] \quad - (17)$$

$$\dot{\epsilon}_2 = \frac{\dot{\epsilon}_e}{\sigma_e} \left[\frac{(\sigma_2 - \sigma_3) - (\sigma_1 - \sigma_2)}{2} \right] \quad - (18)$$

$$\dot{\epsilon}_3 = \frac{\dot{\epsilon}_e}{\sigma_e} \left[\frac{(\sigma_3 - \sigma_1) - (\sigma_2 - \sigma_3)}{2} \right] \quad - (19)$$

σ_e and $\dot{\epsilon}_e$ are the equivalent stress and equivalent strain rate in simple tension.

The stress equivalence is defined as the characteristic physical effect produced in the material's microstructural behaviour which is identical in nature to the datum condition. This datum condition is identified as simple tensile or simple torsion test behaviour. Traditionally this datum has been established in terms of the transition from the elastic to the plastic phase of strain. Creep is a continuous plastic flow phenomenon and therefore it is not sufficient to accept mere transition effect as grounds for equivalence. It is essential to correlate, individually, the different effects of these two stress states (tension and torsion) so that they correspond in both the transition effect and the subsequent time-dependent strain behaviour.

The Von Mises hypothesis which is based on limiting shear resilience defines the equivalent stress as the stress state necessary to bring about the onset of plastic yield. This criterion, at large, has also been accepted for the subsequent plastic flow behaviour and for the equivalence analysis, irrespective of micro-structural and environmental conditions. Increasing stresses and corresponding strains lead to work hardening. At elevated temperatures, this leads to ageing,

precipitation hardening and at some temperatures and limiting soak periods, to thermal softening. Such occurrences may well affect the basic assumptions of isotropy and homogeneity. The effects of environmental factors such as temperature, ageing, precipitation and thermal softening on the test material RR58, are discussed in a later chapter. However, the mathematical relation for equivalence may be summarised at this stage by

$$\dot{\epsilon}_{ij} = C e^{\sigma_{ij}} \quad - (20)$$

where the stress tensor σ_{ij} is the unique stress which is compatible with the strain and strain rate tensors ϵ_{ij} and $\dot{\epsilon}_{ij}$ and are in agreement with the criterion of Von Mises. According to the laws of classical plasticity, the distortion of a material leading to flow produces no change in volume. This means that the relation between the principal strains and correspondingly principal strain rates must be as follows:

$$\epsilon_1 + \epsilon_2 + \epsilon_3 = 0 \quad - (21)$$

$$\text{and} \quad \dot{\epsilon}_1 + \dot{\epsilon}_2 + \dot{\epsilon}_3 = 0 \quad - (22)$$

This in turn implies that

$$\epsilon_1 = \frac{1}{E} \left[\sigma_1 - \frac{1}{2} (\sigma_2 + \sigma_3) \right] \quad - (23)$$

where $\frac{1}{2}$ = Effective plastic flow Poisson's ratio.

It is also assumed that principal shear strains rates are proportional to principal shear stress. Therefore

$$\frac{\dot{\epsilon}_1 - \dot{\epsilon}_2}{\sigma_1 - \sigma_2} = \frac{\dot{\epsilon}_2 - \dot{\epsilon}_3}{\sigma_2 - \sigma_3} = \frac{\dot{\epsilon}_3 - \dot{\epsilon}_1}{\sigma_3 - \sigma_1} = \text{Constant} \quad - (24)$$

and using equations (16), (22) and (24)

$$\dot{\epsilon}_1 = \frac{\dot{\epsilon}_e}{\sigma_e} \left[\sigma_1 - \frac{1}{2} (\sigma_2 + \sigma_3) \right] \quad - (25)$$

With the stated assumptions, the equivalent stress can be expressed in the following forms for a multiaxial system.

$$\sigma_e = \frac{1}{\sqrt{2}} [(\sigma_1 - \sigma_2)^2 + (\sigma_2 - \sigma_3)^2 + (\sigma_3 - \sigma_1)^2]^{1/2} \quad - (26)$$

$$\sigma_e = \frac{1}{\sqrt{2}} [(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + 6(\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2)]^{1/2} \quad - (27)$$

The detailed derivation of equations (26) and (27) is given in most of the standard texts on theory of Elasticity. A particularly exhaustive analysis is dealt with in the texts of Hill¹⁷ and Finnie and Heller¹⁸.

For the test state of the present work, if Von Mises' invariant as stated in equation (27) holds, then the following expression will develop:

$$\sigma_e = [\sigma_x^2 + 3\tau_{xy}^2]^{1/2} \quad - (28)$$

The applied stresses are defined with respect to coordinate axes X, Y and Z

$$\text{where } \sigma_y = \sigma_z = 0 \text{ and } \tau_{yz} = \tau_{zx} = 0$$

Correspondingly on the work principle described later in this chapter, the equivalent strain rate can be expressed as:

$$\dot{\epsilon}_e = \frac{\sqrt{2}}{3} [(\dot{\epsilon}_x - \dot{\epsilon}_y)^2 + (\dot{\epsilon}_y - \dot{\epsilon}_z)^2 + (\dot{\epsilon}_z - \dot{\epsilon}_x)^2 + \frac{3}{2}(\dot{\gamma}_{xy}^2 + \dot{\gamma}_{yz}^2 + \dot{\gamma}_{zx}^2)]^{1/2} \quad - (29)$$

and, when applied to combined tension and torsion condition only, it reduces to:

$$\dot{\epsilon}_e = [\dot{\epsilon}_x^2 + \frac{\dot{\gamma}_{xy}^2}{3}]^{1/2} \quad - (30)$$

Survey of the Effects of Ageing, Anisotropy and Precipitation on Theories of Equivalence

The concepts for complex stress creep behaviour put forward by Bailey, Marin, Odquist and Soderberg - reviewed by the late Dr. A. E. Johnson - are based on stress deviators. Bailey suggests that the creep rate equation is

as follows:-

$$\dot{\epsilon}_1 = A [\sum (\sigma_i - \sigma_2)^2]^m [(\sigma_1 - \sigma_2)^{\overline{n-2m}} - (\sigma_3 - \sigma_1)^{\overline{n-2m}}] \quad - (31)$$

n and m are constants

The first term is a power function of Von Mises' invariant and the second term is some function of the stress deviator. In its simplest form this equation will compare with equation (13). Equation (31) accepts anisotropy and some influence of hydrostatic stress depending upon the degree of work hardening but does not include any effect of ageing and thermal softening.

Kanter and Nadai have included the effect of ageing and thermal softening by treating these phenomena as a variation in viscosity. Kanter suggests that:-

$$\phi \dot{\epsilon}_1 = [(\sigma_1 - \sigma_2) - (\sigma_3 - \sigma_1)] e^{c[(\sigma_1 - \sigma_2) - (\sigma_3 - \sigma_1)]} \quad - (32)$$

where ϕ is the viscosity function. In fact ageing and thermal softening have been lumped together in ϕ as a modification factor while creep behaviour is still only defined by the stress deviator ($\sigma_{ij} - \delta_{ij} \sigma$). It satisfies a theoretical explanation but has been little used in practice.

All these equations, proposed by the authors named, can be summarised as follows: that creep as a plastic flow phenomenon is dependent on deviatoric stress according to the Von Mises hypothesis. The relationships stated are amended for individual requirements to take into account time hardening, ageing, thermal softening and anisotropy. Equivalence between the datum condition and the complex stress state assumes that the environmental, structural and thermal states are compatible. The temperature defines the internal energy and depending upon its magnitude above a limiting temperature, it will accelerate and magnify the resulting strains and the strain rates. Hence it is sufficient to state at this moment that the equations (28) and (30) will have to be modified as stated below.

$$\sigma_e = [\sigma_x^2 + 3 K_0 \tau_{xy}^2]^{1/2} \quad - (33)$$

and $\dot{\epsilon}_e = [\dot{\epsilon}_x^2 + \frac{1}{3 K_1} \dot{\gamma}_{xy}^2]^{1/2} \quad - (34)$

where K_0 and K_1 constant parameters for a given temperature can be expressed as a function of T the absolute test temperature and T_0 the absolute equicohesive temperature.

Effect of Prestrain on Subsequent Creep

Continuing plastic deformation, defined as creep, can be regarded as 'time hardening' or 'strain hardening'. This aspect has been explicitly dealt with by Sully¹⁹ and Finnie and Heller¹⁸ and it is sufficient to refer to their texts for details.

The work of McComb²⁰ and Mair and Pugh²¹ on corresponding equivalence at a strained state is however of note. They have confirmed that the increase of yield stress in a uniaxial load system produces a comparable increase in the yield stress for a combined and complex load system. Therefore the effect on straining behaviour produced by pre-straining or work hardening in a varying complex stress combination will not differ from that of a comparable uniaxial load system. Subsequently the initial equivalence equations will be valid for the overall strain behaviour.

Effect of Rotating Principle Axes

If limiting shear strain energy determines the yield and subsequent plastic flow behaviour then, according to the Von Mises hypothesis, two facts emerge. One is that the hydrostatic stress has no effect on yield and two that the limiting deviatoric stress vector lies in the plane π which is defined by Prager. The limiting value of the deviatoric stress vector, for yield, traces a circle in this plane. The work, of Ivey²² on aluminium alloys and copper, has shown that with increasing stresses under combined loads, the yield locus convexity increases more rapidly in the direction of the increasing deviatoric stress vector.

Hsu²³ states that under such conditions the principal axes rotate and therefore the yield stress vector must alter. The subsequent strain behaviour should alter as well. Hence the strain behaviour for varying stress states under combined loading is not compatible with the corresponding strain behaviour in simple tension or simple torsion for the equivalent stress when the principal axes are stationary,

In the current tests, however, the rotation of principal axes is restricted to a 45° segment of the plane π i.e. it is bounded by simple tension and simple torsion stress states. The variation in the yield locus convexity is small. The effect of soak periods and subsequent thermal softening nullifies any work hardening that occurs. The resulting overall effect on strain behaviour is negligible and cannot be detected.

Mathematical Analysis of Equivalence and The Mode of Creep Behaviour in ALUMINIUM RR58 at Varying Stress and Elevated Temperatures

This subsection for a mathematical analysis is developed from the theoretical stand point of strain behaviour. The criterion for stress equivalence is based on the Von Mises hypothesis and any effects of thermal softening, prior strain and polygonization, which occur at elevated temperatures, have to be included by appropriately reasoned correction factors. The analysis developed falls into three distinct parts:

- (1) The Stress equivalence
- (2) The strain equivalence based on the work hypothesis
- (3) The complete analysis of secondary creep behaviour under increasing stresses that produce increasing prior strains.

Such a relationship, if creep is energy activated, should correlate prior energy of distortion and the subsequent creep work absorption rate.

Stress Equivalence

Stress as a strain producing parameter can be defined as unique, when for a specific material and environmental condition, it produces a special effect. For instance, if for such a stress the material shows a sudden change from elastic to plastic strain behaviour then any other combination of stresses which produce the same behaviour are said to be equivalent. Each stress level produces a certain magnitude of strain from which the stress-strain relationship is established. Hence any stress, in simple tension, after the transitional yield, can by the stress-strain relationship be equated with any given stress combination. This equivalence must therefore be the same as that for the initial yield unless a new mode of dislocation has ensued in the subsequent stages.

The works of Orwan²⁴ and Mott and Nabarro²⁵ relate primarily to transient creep in which the strain is produced by the movement of low energy dislocations. As these dislocations move into high energy equilibrium positions (i.e. as they reach the limiting activation energy) any further creep can only occur by a further nucleation and birth of new dislocations. These physical features can also be created by an increase of stress and the strain it produces. Mott and Nabarro²⁵ have finally shown that the resulting strain ϵ in a single crystal of volume V is given by

$$\epsilon = v \int_0^{\sigma} N_0(\sigma_i) d\sigma_i \quad - (35)$$

where the applied stress $\sigma > \sigma_i$ the internal stress, $\frac{v}{V}$ is the movement of one dislocation loop and N_0 is the dislocation distribution function. They extend the relationship to cover larger stresses in which the activation energy changes to the energy of self diffusion. The level of energy still relates to the applied stress and the shear strain energy criterion for yield still holds for stress equivalence.

It can thus be concluded that the stress equivalence in equation (27) is valid for steady state creep in the following cases:-

- (a) Low stress levels at which the creep rate obeys the power law of equation (5)
- (b) High stress levels at which the creep rate follows the exponential form of equation (11) provided that the ageing and precipitation effects are negligible.

The shear strain energy criterion for yield and subsequent flow implies that the equivalent stress σ_e in simple tension produces the same value of octahedral stress as that produced by certain magnitudes of combined loading. This does not include deterioration of the material matrix or the coarsening of the grain structure. The work of Clark and White²⁶ shows that coarse grained material is more creep resistant at high temperatures while fine grained material is more resistant at low temperatures. At the high stress levels and elevated temperatures employed - these temperatures in the present tests are in excess of the equicohesive temperature for the test material, HIDUMINIUM RR58 - the grain structure coarsens. This occurs in the present work in association with heavy precipitation, polygonization and recrystallization together with prior strain. Cottrell¹⁰ and Sully¹⁹ have discussed these aspects at great length in their texts on dislocations and metallic creep. They state that such effects will accelerate subsequent creep rates. Cottrell goes on to point out that when a dislocation moves in a slip band, the work done in the process by the applied stress, is used up mainly in an increase of the kinetic energy of the dislocation and is marginally dissipated by thermo-elastic damping, radiation damping and scattering sound waves. The material at high temperatures becomes thermodynamically unstable and creep strains are accelerated.

In a thermodynamic process the total energy is given by the expression

$$\int P dv + C_v T + U_{\text{Potential}} + U_{\text{Kinetic}}$$

where P = press., dv = volumetric change

C_v = specific heat at constant volume

and T = absolute temperature

If this reasoning is applied to the creep process where no change in volume occurs, the extra energy can only be accommodated by increasing substructural mobility.

The aggravation can only occur in the energy component which is initiated by the externally applied shear stresses. If such a mode of behaviour in the material structure does not occur below equicohesive temperatures, the magnification factor in this energy component must be a ratio of the absolute operating temperature and the absolute equicohesive temperature for the test material.

Hence the σ_e in equation (27) adopts the form:

$$\sigma_e = \frac{1}{\sqrt{2}} \left[(\sigma_x - \sigma_y)^2 + (\sigma_y - \sigma_z)^2 + (\sigma_z - \sigma_x)^2 + 6 \frac{T}{T_0} (\tau_{xy}^2 + \tau_{yz}^2 + \tau_{zx}^2) \right]^{1/2} \quad - (36)$$

This equation, when it is applied to combined tension-torsion becomes:

$$\sigma_e = \left[\sigma_x^2 + 3 \frac{T}{T_0} \tau_{xy}^2 \right]^{1/2} \quad - (37)$$

where $\sigma_y = \sigma_z = \tau_{zx} = \tau_{yz} = 0$

and, for relating simple tension and simple torsion, reads

$$\sigma_e = \sqrt{3 \frac{T}{T_0}} \tau_{xy} \quad - (38)$$

Strain Equivalence and Strain Rate Equivalence

The applied stresses in experimental and service conditions for specific materials and prevailing environmental conditions give certain magnitudes of plastic strains. In the secondary creep phase the plastic strains are directly proportional to creep rates, hence the strain rate equivalence

relationship can be established from the strain increment equivalent equation.

The strain increment behaviour has been very comprehensively discussed by Hill¹⁷ and Nadai²⁷. This is summarised in the normality rules of plasticity; firstly that in a plastic flow process the plastic work is positive, i.e. $\sigma_{ij} d\epsilon_{ij}^p > 0$ and secondly that there exists a loading surface where the plastic strain increment is proportional to a function of the stress tensor increment, i.e.

$$d\epsilon_{ij}^p \propto f(d\sigma_{ij}) \quad - (39)$$

Hence in a time-dependent process there is a need to establish the σ_e vs $\dot{\epsilon}_e$ characteristic positively where $\dot{\epsilon}_e$ represents an equivalent strain rate for any combination of applied stresses.

Two hypotheses are postulated for such a derivation. The first is the strain hardening process which relates σ_e to a certain measure of the total plastic deformation $\int d\epsilon_e^p$. This measure is a function $H(\int d\epsilon_e^p)$. The equivalent plastic strain $d\epsilon_e^p$ must be directly proportional to σ_e because no change in volume occurs, i.e.

$$d\epsilon_{ii}^p = 0 \quad - (40)$$

$$\text{Hence } \sigma_e = H(\int d\dot{\epsilon}_e^p) \quad - (41)$$

The function $H(\int d\dot{\epsilon}_e^p)$ can be determined from simple tensile creep testing.

The static tensile and static torsion tests conducted at elevated temperatures (above 100°C) give a very flat curve for stress vs strain plots beyond yield. In addition it has already been indicated that the thermodynamic effects involved at the operating temperatures in the tests nullify the effect of strain hardening. Therefore, the strain hardening hypothesis is not likely to be valid.

The second hypothesis is the work hypothesis. It relates the equivalent stress σ_e to the work done in the process of plastic deformation and can be expressed in the form

$$\sigma_e = F(W_p) \quad - (42)$$

where the total plastic work W_p equals $\int \sigma_{ij} d\epsilon_{ij}^p$

The function $F(W_p)$ can be determined from simple tensile testing. During steady state creep the strain increments are directly proportional to the creep rate and therefore

$$\sigma_e = F_1 \int \left(\frac{dW_p}{dt} \right) = F_1 \left(\int \sigma_{ij} d\dot{\epsilon}_{ij}^p \right) \quad - (43)$$

For simple tension, in the plastic range, the stress σ_x in the X direction equals σ_e and also

$$\sigma_e = F \left(\int \sigma_x d\epsilon_x \right) \quad - (44)$$

Whilst in simple torsion when

$$\sigma_e = K \tau_{xy} \quad \text{where } K = \text{constant}$$

$$\sigma_e = F \left(\int \tau_{xy} d\gamma_{xy} \right) = F \left(\int K \tau_{xy} \frac{d\gamma_{xy}}{K} \right) \quad - (45)$$

If the argument F is the same in both the cases, the graphs of σ_x against $\int d\epsilon_x$ and $K \tau_{xy}$ against $\int \frac{d\gamma_{xy}}{K}$ are coincident. It has been shown in the preceding section that $\sigma_e = \sqrt{3 \frac{T}{T_0}} \tau_{xy}$ or $K = \sqrt{3 T/T_0}$

To check the validity of this relationship the static tension and static torsion data were used. For test temperatures higher than 100°C , 0.1, 0.2 and 0.5 per cent tensile proof stresses were derived. (The 0.1 per cent proof stress is the stress level which produces 0.1 per cent permanent plastic strain). Correspondingly the equivalent shear stresses were derived from the static torsion tests using the equivalent shear strains as $0.1 \sqrt{3 T/T_0}$, $0.2 \sqrt{3 T/T_0}$ and $0.5 \sqrt{3 T/T_0}$ per cent.

From the stress equivalence equation

$$\frac{\sigma_e \text{ in simple tension}}{\sigma_e \text{ in simple torsion}} = \frac{\sigma_x}{\sqrt{3 \frac{T}{T_0}} \tau_{xy}} = 1$$

$$\text{or } \frac{\sigma_x}{\tau_{xy}} = \sqrt{3 T/T_0}$$

Therefore from the values determined σ_x/τ_{xy} for each temperature was averaged and plotted in Fig. 18 to a base of $\sqrt{T/T_0}$. The slope of $\sqrt{3}$ confirms the validity sought.

In order to generalise the stress equivalence, consider the work hypothesis as applied to combined tension-torsion tests.

$$\begin{aligned} dW_p &= \sigma_e d\epsilon_e^p = \sigma_x d\epsilon_x + \tau_{xy} d\gamma_{xy} \\ &= \sigma_x d\epsilon_x \left(\sqrt{1 + \frac{\tau_{xy}}{\sigma_x} \frac{d\gamma_{xy}}{d\epsilon_x}} \right)^2 \end{aligned} \quad - (46)$$

If $\frac{d\gamma_{xy}}{d\epsilon_x} = \lambda \frac{\tau_{xy}}{\sigma_x}$

and is substituted in the equation, where λ is some constant

$$\begin{aligned} dW_p &= \sigma_x \left(1 + \lambda \frac{\tau_{xy}^2}{\sigma_x^2} \right)^{1/2} d\epsilon_x \left(1 + \lambda \frac{d\gamma_{xy}^2}{d\epsilon_x^2} \right)^{1/2} \\ &= (\sigma_x^2 + \lambda \tau_{xy}^2)^{1/2} \left(d\epsilon_x^2 + \frac{d\gamma_{xy}^2}{\lambda} \right)^{1/2} \end{aligned}$$

It has been shown that under combined tension-torsion

$$\begin{aligned} \sigma_e &= \left[\sigma_x^2 + 3 \frac{\tau}{\tau_0} \tau_{xy}^2 \right]^{1/2} \\ \therefore \lambda &= 3 \tau / \tau_0 \\ \text{and } d\epsilon_e^p &= \left[d\epsilon_x^2 + \frac{d\gamma_{xy}^2}{3 \tau / \tau_0} \right]^{1/2} \end{aligned} \quad - (47)$$

Thus in any combined tension-torsion test the ratio

$$\frac{d\gamma_{xy}}{d\epsilon_x} = 3 \frac{\tau}{\tau_0} \frac{\tau_{xy}}{\sigma_x} \quad - (48)$$

As stated earlier the strain increments are directly proportional to the strain rates, hence the equation (47) can be expressed in terms of strain rates as

$$\dot{\epsilon}_e = \left[\dot{\epsilon}_x^2 + \frac{\dot{\gamma}_{xy}^2}{3 \tau / \tau_0} \right]^{1/2} \quad - (49)$$

Stress-Strain Rate Relationship During the Incremental Stress

Secondary Creep

In the present work, high stress levels have been employed at high temperatures which are in excess of the equicohesive temperature. In the earlier survey and analyses it has been shown that the creep strains and creep rates, activated by the applied stresses lead to increases in the energy of self diffusion.

The energy absorbed increases also in magnitude owing to the effects of ageing, precipitation, recrystallization and polygonization. The structure of the material matrix changes continuously with strain and the thermodynamic processes. All these effects are due to distortion. Hence in the physical sense, the rate of change must relate to some function of the energy of distortion. In the analyses formulated there is an underlying assumption that the hydrostatic stress has no influence; therefore the effective energy of distortion at any stage equals the shear strain energy at the current stress in addition to the distortion work expended in the process of permanent deformation. Mathematically this energy of distortion is expressed as:

$$U_{sh} = \frac{1}{6G} [\sigma_x^2 + 3 \tau_{xy}^2] \quad \text{where } G = \text{elastic modulus of rigidity at the operating temperature}$$

$$W_p = \sum \sigma_{e_i} \epsilon_{e_{i-1}}^p \quad i \text{ has values of } 1 \text{ to } n, \text{ where } n \text{ is the number of loading stages.}$$

[Note: In the test material used, the elastic strains are of significant magnitude. Therefore, the elastic shear strain energy is significant and thus cannot be ignored]

If the accelerated creep rates at operating temperatures and stresses are produced by an increase in the energy of self diffusion, a mathematical relationship exists between the creep work absorption rate and the energy stored in the material by virtue of its already distorted state. The work absorption rate is some measure of the decaying strength of the original material. Therefore, when the stored energy (elastic shear plus plastic and creep work) increases, the work absorption rate, which is a measure of the decaying rate owing to creep damage, will accelerate. This leads to the postulation that the creep work absorption rate $\sigma_{e_i} \dot{\epsilon}_{e_i}$ is proportional to some function of the energy of distortion $U_{sh} + W_p$.

It is also suggested that there should be some limiting distortion energy when the equilibrium between thermal softening, strain hardening and recrystallization is turbulently disturbed. Cottrell's comments are relevant, when he states that under conditions of high stresses and strains and unstable thermodynamic conditions, mechanical instability is automatic. Such an occurrence would lead to the development of numerous vacancies and voids which would produce serious crack propagation and rapid rupture.

In the simplest form, for the steady creep phase, the relationship proposed can be expressed as

$$\sigma_{ei} \dot{\epsilon}_{ei} = M (U_{sh} + W_p)^{1/N} \quad - (51)$$

where N is a constant for the specific material and the operating temperature.

M is a constant dependent on time interval for each load stage.

The validity of the proposed hypothesis is strengthened when in the present tests a limiting energy is observed for the onset of the tertiary creep phase and the resulting collapse.

DESCRIPTION OF TEST EQUIPMENT

The test programme represents an attempt to establish the uniqueness of creep behaviour of the test material HIDUMINIUM RR58 DTD 731 aluminium alloy at elevated temperatures. Manufacturers, of the test material, High Duty Alloys Limited, Slough, state that where as minimum mechanical properties are quoted in various abstracts, the basic and comparator characteristics must be established for each batch of material supplied - aluminium alloys of this type, because of the complexity of the solid solution state, may exhibit 5 to 10 percent variation in mechanical properties.

Hence to propose an experimentally-based mathematical relationship for creep behaviour under complex and incremental stresses at elevated temperatures, combined load tests have to be carried out.

Static tensile characteristics and simple tensile creep data have been obtained from tests carried out on a standard $\frac{3}{4}$ ton Denison Creep Tester. For static, torsion characteristics, simple creep tests and combined tension-torsion creep tests, a complex creep testing machine was designed and employed.

$\frac{3}{4}$ Ton Denison Tensile Creep Tester and Test Specimen

This is a single lever (10 : 1 ratio) direct loading machine - type 47D. It has a three zone standard muffle furnace. The original temperature working range was 400°C to 900°C but for accurate temperature control to within $\pm \frac{1}{2}^{\circ}\text{C}$, it was modified by Webster²⁸ using a saturable reactor-type supply and controller. It is comprehensively described in his thesis referred to in the bibliography.

The standard test specimen used in this type of machine was redesigned to suit extensometer No. 2 described in the appendix and the details are shown in Fig. 6. The one inch gauge length specimens used in the past have suffered from two difficiencies (i) lack of alignment- (2) inaccurate gauge length and extensometer location.

The threaded and spigot ends provide good location and alignment. The thread profile ridges at the ends of the gauge length define it accurately. This type of location was first employed by the Creep Division of Royal Aircraft Establishment, Farnborough, and the details, as shown in Fig. 29.12. of the appendix, do not induce any variation in the stress distribution along the gauge length:-

Complex Creep Testing Machine

Since no suitable machine existed the apparatus for this test programme had to be specially designed and calibrated. A technical paper describing this work was presented to the Joint convention of the Society of Environmental Engineers and the Institution of Mechanical Engineers in September 1965 at the Institute of Science and Technology of the University of Manchester. To save repetition, a copy of this paper is attached as an appendix.

Early exploratory tests on the test material revealed very small axial and torsional strains and it became necessary to record the axial displacement of 0.001 in. magnitude over 10 hours and torsional strains of 0.001 degree over a similar period. This necessitated some modification to the machine.

Extensometer No. 1 (Modified)

Extensometer No. 1 as described in the appendix is modified to accommodate transducers which cannot be enclosed within the furnace. A spider plate was secured to each of the locating rings and three dog-leg extensions were secured in the milled slots of the spider plates as shown in Fig. 7.

The dog-leg extensions are sufficiently long to project from the furnace. The lower ends of these extensions are secured in the milled slots of the indexing rings. The upper indexing ring is secured to the upper locating ring. It also carries two end stops. One is for the axially mounted transducer (for direct strains) and the other stop is for the horizontally mounted transducer (for torsional strains). The axial stop is shaped like a mushroom-type follower with a threaded stem while the horizontal stop is similarly shaped but is attached to a one inch micrometer spindle at a suitable radius from the axis of the machine. The transducers are mounted from the lower indexing ring.

The adjustable stops facilitate repositioning the transducers so that at no time during the tests are they in the non-linear portion of their stroke.

The transducers are of the differential transformer type. These 'C.N.S.' transducers are precision wound solenoid coils on quartz formers. The recording system comprises two matched transducers working in unison, automatically averaged to a suitable scale and fed into an autochart recorder. Only one transducer could be used in the extensometer while the other was left in the locked position. A master transducer located in the amplifier is used as a null balance. The amplifier has six sensitivities to give full scale displacement ranges from 0.25×10^{-3} to 100×10^{-3} inch. The accuracy of measurement obtainable is $\pm 5.0 \times 10^{-6}$ inches.

For ease of assembly and location of the extensometer, the locating probes were drilled and tapped. The extension stems with knurled ends were screwed into these probes. The housings were tapped and circlips were replaced by grub type screws with a central hole. This arrangement eases the aligning of the probe grooves on the gauge length ridges. While the probe is held, the spring force can be increased by tightening the grub screw. In order to obviate thermal distortion, all parts were made from high temperature steel, EN 58 B.

For the 4.0 in. gauge length specimens the accuracy of measurement for axial strains is 2.5×10^{-6} and for torsional strains is 0.5×10^{-6} .

Furnace Modification and Recalibration (Ref. Appendix Fig. 29. 15)

Owing to extensometer modification, the 'Sindanyo' furnace base-plate was bored out to $3\frac{1}{2}$ in. diameter. The radial probes were removed. The ports used for viewing and illumination, were plugged with asbestos floss to obviate heat loss. The 24 thermocouple dummy specimen, with the extensometer assembled in the test position, was put in the test machine. The furnace was recalibrated at 100°C and 200°C - the temperature distribution obtained in the calibration is shown in Fig. 8.

Instrumentation, Control and Strain Calibration

The Denison creep tester control has been discussed by Webster²⁸. The modified extensometer is described in the appendix where it is listed as extensometer No. 2. The furnace control sensitivity was set at 0.75 for the temperature range 100°C to 250°C . The control setting was adjusted for appropriate temperature and controlled by intermittent trim to within $\pm \frac{1}{2}^{\circ}\text{C}$.

Three chrome-alumel thermo-couples were fixed to the one inch specimen at $\frac{1}{2}$ in. intervals along its gauge length. The welded junctions were held to the specimen with asbestos paper and tied by copper wire. The induced EMF was measured using a 'CROPICO' potentiometer type P.3 and a multipole thermocouple box. The cold junction was held in crushed ice. The potentiometer has three ranges 1.8V, 0.18V and 0.018 volts and has a measurable accuracy of ± 5.0 microvolts. It has a 2.0 volt stabilised supply and therefore during temperature recording a control of $\pm 0.1^{\circ}\text{C}$ can be maintained.

The test equipment is housed in a temperature controlled laboratory which is maintained between 19° to 20°C . The equipment rests on a vibration proof floor so that the effect of any environmental disturbance is minimised.

The control for the complex creep testing machine furnace is identical, except that 5 thermo-couples are attached to the test specimens along the gauge length at 1.00 in. intervals in a spiral fashion.

In the Denison creep tester, the displacements measured were the actual strain readings and no conversion was necessary.

In the complex creep machine, the actual readings have to be doubled because only one transducer of a dual system is operating. The gauge length is 4 inches, hence the actual readings of displacement thus obtained have to be suitably divided to determine engineer's axial strains. The torsional displacements also have to be doubled and converted into shear strains.

SELECTION OF TEST MATERIAL

It was envisaged that the test programme should produce an experimentally backed mathematical theory for creep behaviour. This creep behaviour has to be related to complex and incremental stress conditions at elevated temperatures.

In order to produce experimental data for engineering materials in common use and with a view to providing a general analysis, the following materials were considered:

1. Commercial Steels
2. Copper and its alloys
3. Magnesium based alloys
4. Aluminium based alloys

From the outset it was realised that any individual test run for creep will be conducted at accelerated rates hence high stresses at correspondingly high temperatures are inevitable. The test material should have uniformity of structure, isotropic behaviour, and thermal stability. These considerations precluded the use of steels. Copper is rarely used as a load carrying material and therefore even if it tends to satisfy nearly all the conditions named, it must be omitted.

Aluminium and Magnesium alloys are of interest. Magnesium alloys have good strength but require stringent machining control. They also form oxide films at elevated temperatures which may produce reinforcement in the thin walled tubular test specimens. Hence it was decided to use an aluminium alloy as the test material. More expert advice was sought with regard to engineering applications with particular reference to desirable properties.

The late Dr. A.E. Johnson of NEL, East Kilbride was consulted. It was decided to use a newly developed material which was finding special favour as an aircraft material for the new generation of supersonic planes in which creep behaviour is of serious consequence. **HIDUMINIUM RR58** aluminium alloy

manufactured by High Duty Alloys Ltd. of Slough was considered. Little or no work has been done on this material under complex and incremental stress creep at high temperatures.

Dr. Johnson in the light of his extensive experience agreed with this choice of test material and recommended two alloys, RR59 and RR58. There are a whole series of DTD specifications and it was pointed out that forged stock would give the best uniformity of mechanical properties. It is also likely to give insignificant anisotropy.

RR59 and RR58 aluminium alloys are manufactured by High Duty Alloys Ltd. under their commercial designation 'HIDUMINIUM' alloys according to various specifications, in the form of forgings, extrusions and clad sheets. Two billets, one of RR58 and the second of RR59, 2½ in. diameter and each 2 feet long were supplied by the manufacturers for selection tests. The specification and nominal compositions are:-

HIDUMINIUM RR59

SPECIFICATION B.S. 3L42.E. forging-fully heat treated

NOMINAL COMPOSITION % by weight

Copper	2.2	Magnesium	1.5	Nickel	1.2
Iron	1.0	Silicon	0.85	Titanium	0.1

HIDUMINIUM RR58

SPECIFICATION DTD 731 forging W.P. fully heat treated

NOMINAL COMPOSITION % by weight

Copper	2.5	Magnesium	1.5	Nickel	1.2
Iron	1.0			Titanium	0.1

And the details of heat treatment are respectively.

RR 59

Solution treated, 6 hours at 530°C

Quenched in boiling water

Aged for 16 hours at 173°C

RR 58

Solution treated, 20 hours at 530°C

Quenched in boiling water

Aged for 20 hours at 200°C

Manufacture of Tubular Test Specimen

The details of the tubular specimen are shown in the appendix Fig. 29.2. The forged stock was rough machined into a 2 1/32nd in. diameter and 9¼ in. long blank. It was further machined between centres so that the allowance over the parallel portion of the gauge length was 3/16 of an inch. It was bored out to 0.994 in. diameter in three decreasing cuts and finally reamed to size with a precision machine reamer. The blank was finally finish-machined, mounted on a 1.00 inch mandrel, in four successive cuts. The cuts were 0.004 in. and 0.0015 in. respectively using a particularly sharp tool edge. The use of sandcloth for polishing specimen surfaces - quite a common practice among machinist - was prohibited.

This machining procedure ensured that any machining stresses, induced, were kept to a minimum.

Selection Tests

From each billet sample, two tubular specimens were machined as described above. RR59 specimens have been labelled A and C and the RR58 are B and D.

The specimens A and B were used for static tensile tests to fracture at room temperature. The experimental results are given in Table I and plotted in Fig. 9.

The second pair, specimens C and D, were used for incremental stress tensile creep at room temperature. The results are shown in Table II and plotted in Figs. 10 and 11.

Test Analysis and Conclusions

Static tests: Both specimens exhibit identical elastic behaviour. RR59 has a marginally flatter plastic phase.

Incremental Stress Creep: The test set up is shown in Fig. 29.10. of the appendix. The specimen D was loaded for creep to a higher stress value than specimen C to achieve similar initial plastic strain. The first stage loads were of one week duration. The RR58 material showed a very small creep and subsequent load increments gave a total creep strain of 0.3 per cent while RR59 with one less load stage and smaller load increments over a similar period produced a total creep of 1.0 per cent only.

Static tests indicated that the working range for incremental stresses lay between 0.1 and 2.0 per cent strains. Beyond 2 per cent strain accelerated creep rates lead rapidly to fracture. Moreover beyond this range, marginal stress variation produced extremely high creep rates and the accuracy of their magnitude was questionable.

RR 58, owing to the absence of silicon, is more resistant to creep. A slightly larger stress working range made it the superior of the two materials.

For creep rate variation from 1×10^{-6} to 400×10^{-6} per hour, the stress ranges at room temperature are:-

RR 59 20.5 tonf/in² to 23 tonf/in²

RR 58 23.0 tonf/in² to 26.0 tonf/in²

On the basis of this nominal assessment HIDUMINIUM RR 58 - DTD 731 material was selected.

Test Material Specification and Requirements

HIDUMINIUM RR 58 (DTD 731) as a test material should have uniformity of structure and of mechanical properties. This demands that the entire test stock should come from a single ingot. The ingot was initially produced in a large billet and was subsequently hot forged to the nominal size required. During the forging process the billet section was reduced to a 1/15th of the original cross sectional area.

Although the entire stock came from the same melt, it could not be forged from a single ingot. Therefore two ingots were used. It was supplied in 2.5/8th in. nominal diameter stock in five foot lengths.

The manufacturers stated that the material RR 58 did not require further heat treatment. Any such treatment would only alter the solid solution equilibrium, the grain size and might seriously denude the matrix of precipitate and intermetallic compound dispersion.

PRELIMINARY TESTS

Simple data for the test material - RR 58 - obtained from a preliminary search is given in the Tables III and IV. It was supplied by the courtesy of the Research Division of High Duty Alloys Ltd.

To establish the assumptions made and the reliability of the data, it was essential to select test temperatures, stress ranges, and also check the isotropic behaviour. The late Dr. A. E. Johnson has pointed out that isotropic compatibility based on static tests is insufficient and has to be checked by creep tests. Meanwhile it is to be remembered that copper based aluminium alloys exhibit considerable scatter in mechanical properties.

Three groups of tests were carried out with the above factors in view:

- 1) Static tests - simple tension and simple torsion at various temperatures.
- 2) Isotropic compatibility in creep tests.
- 3) Stress range selection for creep tests at elevated temperatures for stress increments in the main test programme.

The tensile tests were conducted in the $\frac{3}{4}$ ton standard Denison tensile creep tester. The test specimens have 1.00 in. gauge length and a 0.1875 in. solid diameter. The torsion tests are carried in the complex creep machine using 4.00 in. gauge length tubular specimens.

Manufacture of 1.00 in. Gauge Length Specimens

Seven blanks were obtained from each test stock section axially and were first turned parallel to $\frac{9}{16}$ in. diameter between centres. The ends were finish machined and threaded while the mid-span was reduced to a $\frac{5}{16}$ in. diameter.

With a sharp tool the mid portion was further reduced to $\frac{1}{4}$ in. diameter and was finally finish-machined with three cuts of 0.015, 0.008 and 0.002 in. successively, using a copying attachment on a lathe.

Static Tests - Tensile and Torsion

Data supplied by High Duty Alloys Ltd. in Tables III and IV shows that mechanical properties are adversely affected by longer soak times at high temperatures. For torsion tests and tensile tests the respective furnaces were allowed a stabilising period of 6 hours to reach and to settle at the test temperature (it was however observed that furnaces for both the test machines stabilised in 4 hours approximately).

Static tensile tests are recorded in Table V and plotted in Figs. 12 and 13 while the torsion tests are shown in Table VI and Figs. 15 and 16.

Variation of tensile and torsional properties with temperature as derived from the tests above are shown in Figs. 14 and 17 respectively.

Data shown in tables III and IV and the experimental results obtained as recorded in Tables V and VI show that stress dependence follows the criterion of neither Von Mises nor that of Tresca. The dependence has already been examined in the theoretical discussion in the previous section (equations 38 and 47) in which it was formulated that for the torsion test,

$$\sigma_e = \sqrt{3 \tau / \tau_0} \tau_{xy}.$$

and

$$\epsilon_e = \gamma_{xy} / \sqrt{3 \tau / \tau_0}$$

In Table VII(b) 0.1, 0.2 and 0.5 per cent direct proof stresses and $0.1 \sqrt{3 \tau / \tau_0}$, $0.2 \sqrt{3 \tau / \tau_0}$ and $0.5 \sqrt{3 \tau / \tau_0}$ per cent permanent shear strain stresses are listed.

The average ratio of direct proof stress and corresponding shear stress is plotted to a base of $\sqrt{T/\tau_0}$ in Fig. 18. The slope $\sqrt{3}$ confirms the validity of the equations of stress and strain equivalence.

Isotropy Tests

RR58 shows a serious drop in mechanical properties with increasing temperatures beyond 100°C . Hence the test temperatures chosen are 100° , 150° and 250°C . Nine specimens were used for simple tensile creep along various orientations to check and confirm isotropic behaviour.

The specimens were selected in orientation with reference to the axis of forging as shown in Fig. 5. The appropriate specimens are tested for constant stress tensile creep at the three temperatures. The creep behaviour is shown in Figs. 19, 20 and 21. The analysis of the tests is given in Table VIII. The details of tests are as follows:

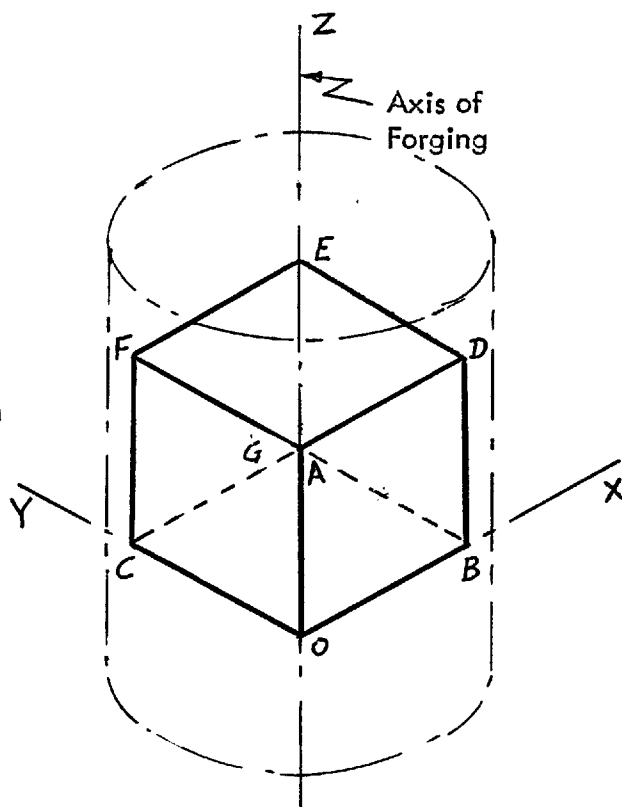


Fig. 5

Temperature °C	Direction	Specimen Code	Stress Tonf/in ²	Diameter ins.
100 ± 0.4	OA	Z ₁	23.60	0.1872
	OB	X	23.55	0.1875
	OC	Y	23.52	0.1880
150 ± 0.4	OA	Z ₂	23.52	0.1878
	AG	ZXY _{ag}	23.55	0.1875
	OE	ZXY _{oe}	23.71	0.1883
250 ± 0.4	OA	Z ₃	12.10	0.1878
	CD	XZY _{cd}	12.05	0.1875
	FB	XZY _{fb}	12.16	0.1878

The specimen dimensions were measured on the MU 214B Universal Measuring Machine manufactured by 'Societe Genevoise d'Instruments de Physique' to the nearest 0.0001 of an inch. The smallest load used on the 10 : 1 loading lever was $\frac{1}{2}$ lbf and this accounts for the slight variation in comparable stress levels. The scatter in creep at any one temperature was considered within the material variation and it was therefore concluded that the material behaved isotropically.

Incremental Stress Tensile Creep (1in. gauge length specimens)

Isotropy tests were conducted at 100°C because this temperature is below the equicohesive temperature for the material. At the higher temperatures, scatter in creep behaviour can be seen in Figs. 20 and 21. Incremental stress tensile creep tests were carried out at 150°, 200° and 250°C as essential preliminary tests. A total of 32 specimens so tested were as follows:

Test Temperature °C	No. of Specimens used
150	8
200	12
250	12

The tests were conducted in the $\frac{3}{4}$ ton Denison tester and each specimen was allowed a 24 hour soak time before loading was commenced. The experimental results are given in Tables IX, X and XI.

Some experiments were seriously affected by the environmental vibration due to a fatigue test programme in the vicinity, loss or variation of power supply and inadequate temperature control and therefore they have been omitted. The temperature for the tests recorded was maintained within $\pm 0.5^{\circ}\text{C}$ by trimming furnace controls when necessary.

In incremental loading tests the secondary creep rates did not correspond to a power law but fitted a law of the form

$$\dot{\epsilon}_c = A e^{B\sigma} \quad \text{stated in equation (11)}$$

At 200° and 250°C the scatter was alarming. The exponential plot is shown in Figs. 22, 23 and 24.

In view of the scatter, the energy stored due to shear, plastic and creep strains prior to each secondary creep phase was determined as $U_{sh} + W_p$ and the subsequent rate of work absorption as $\sigma_{ei} \dot{\epsilon}_{ei}$. These values were calculated and are recorded in Tables XII, XIII and XIV. The two new parameters were plotted on the basis that

$$\sigma_{ei} \dot{\epsilon}_{ei} = M (U_{sh} + W_p)^N$$

and are shown in Figs. 25, 26 and 27. The scatter in results plotted on the basis of this later relationship was greatly reduced.

MAIN TEST PROGRAMME

(Tubular specimens)

The onset of tertiary creep phase was observed at all temperatures in the preliminary tests in the proximity of 0.7 per cent creep strain. This dictated that in order to accommodate all stress increments the final stress had to be below the 0.1 per cent proof stress level. The elastic ^{perfectly} plastic (near non-work hardening) behaviour of the test material also imposed very small stress increments. The scatter in the previous tests did not exhibit sufficient repeatability in the smaller test specimens. It was therefore concluded that the incremental stress tensile creep tests were to be repeated in the complex creep testing machine so that all the tests might be compared.

The main programme thus formulated was as follows:

Nature of Tests

Test Temperatures

150°C and 250°C

Each test was to have 5 stress levels, i.e. 4 stress increment stages of 24 hour duration each.

Each specimen had a no load soak period of 24 hours.

Tests

1. Simple incremental tension
2. Simple incremental torsion
3. Combined tension and torsion

(a) $\frac{\sigma_x}{\tau_{xy}} = 2$ at all stress phases

(b) $\frac{\sigma_x}{\tau_{xy}} = \frac{1}{2}$ at all stress phases

4. Combined tension and torsion where, in the first phase $\frac{\sigma_x}{\tau_{xy}} = 1$

(a) σ_x is held constant while τ_{xy} is increased

(b) τ_{xy} is held constant while σ_x is increased

Twelve specimens were used in tests at 150°C and fifteen specimens at 250°C. In tests, where $\frac{\sigma_x}{\tau_{xy}}$ was less than half, no measurable tensile extension could be distinguished and therefore the results are not presented. Some tests were lost due to loss of temperature control or sudden specimen collapse. These tests are also not recorded.

Test Procedure

Each test specimen, with five chroma-alumel thermocouples equispaced along the gauge length, was assembled into the complex creep testing machine with the extensometer clamped in position. The furnace was closed around this assembly. It was ensured that the specimen assembly was free to move vertically and to rotate. The top and bottom furnace openings were plugged with asbestos floss.

The furnace was switched on and during the first four hours considerable control trimming was required to obtain a stable temperature to within $\pm 0.5^\circ\text{C}$. After a 24 hour no-load soak period the specimen was loaded within the elastic range and the moduli of elasticity and rigidity were checked. By this procedure it was established that the specimen assembly was free to float and that the measuring instruments were functioning satisfactorily.

The first load was applied and at the end of each 24 hour creep stage the two load increment components were added simultaneously. These additional loads were placed in position gently.

When the plastic and creep shear strains approached 1.5 per cent most specimens developed, within about 20 minutes, an axial crack in the walls which led to rapid collapse. In some instances the crack propagation was just arrested. Some examples of these cracks and the nature of their subsequent fracture are shown

in Fig. 57. The creep curves for all the tests are shown in Figs. 28 to 48 and the main data derived is recorded in Tables XV and XVI.

The transducers were calibrated against the stop micrometer before the furnace supply was switched on and before loading was commenced. The load cells were calibrated against bucket loads after every two tests.

ANALYSIS OF TEST RESULTS

All the tests conducted are analysed under this heading. Some tests with relatively long duration for each load stage are also quoted for reference.

Selection Tests

The two materials used in the selection tests were RR58 and RR59 aluminium alloys. RR58 gave slightly higher tensile properties and was also more creep resistant than RR59 as shown in Figs. 9, 10 and 11. Lack of comprehensive creep data on RR58 made the selection of this material more attractive. The test specimen dimensions are thought to be critical and were finally chosen after consulting the late Dr. A. E. Johnson of NEL, East Kilbride. He had recommended that the section width must be greater than eight times the average grain size. The average grain width along the specimen cross section in the unstrained state as measured from the micrographs in Figs. 54, 55 and 56(a) and (b) varied between 0.001 to 0.004 inches.

These tests showed, that in order to obtain significant creep strains in incremental stress creep, the stress increments would have to be chosen carefully so that all the stress increment stages, desired in each test, could be accommodated.

Static Tensile and Static Torsion Tests

Nadai²⁷, Drucker²⁸ and Hill¹⁷ have discussed strain behaviour in great detail and have put forward theoretical concepts of equivalence between different types of applied loads. The most popular are the criteria of maximum shear stress and maximum shear strain energy.

The static tests for a temperature range of room temperature to 300°C are recorded in Tables V, VI and VII and are graphically represented in Figs. 12 to 18. The analysis of these results showed that the material obeyed the shear strain energy

criterion of Von Mises in plastic flow behaviour, based on the following relationships up to 100°C [See Table VII (a)]

$$\sigma_e = \sqrt{3} \tau_{xy} = \text{stress in simple tension}$$

$$\epsilon_e^p = \frac{\gamma_{xy}^p}{\sqrt{3}} = \epsilon_x^p \text{ strain in simple tension}$$

The agreement was confirmed when the equivalent shear stresses were derived from the equivalent plastic shear strains which correspond to 0.1, 0.2 and 0.5 per cent direct proof stresses. As the temperatures increased towards 250°C, the stress equivalence approached the maximum shear criterion of Tresca. Beyond this temperature the stress equivalence co-efficient continued to increase. The smooth continuity of the stress vs temperature curves as shown in Figs. 14 and 17 was disturbed in the vicinity of 100°C. The critical temperature, from the graphs mentioned, was optimised as 110°C. The manufacturers had quoted an equicohesive temperature range from 100 to 120°C and therefore the equicohesive temperature was taken as 110°C. A similar discontinuity was observed around 200°C and it can be surmised that this was due to severe thermal softening which occurs at elevated temperatures in the presence of some strain, serious recrystallization and recovery.

In view of the limiting creep strains encountered in the preliminary tensile creep tests, the stress equivalence was investigated for the region where the limiting plastic strain was 0.5 per cent.

The 0.1, 0.2 and 0.5 per cent direct proof stresses were determined for operating temperatures higher than 100°C. Corresponding shear stresses were obtained for $0.1 \times \sqrt{3} T/T_0$, $0.2 \times \sqrt{3} T/T_0$ and $0.5 \times \sqrt{3} T/T_0$ per cent plastic shear strains. At each temperature the average ratio for $\frac{\sigma_x}{\tau_{xy}}$ was determined from the three values above. When this parameter $\frac{\sigma_x}{\tau_{xy}}$ was plotted against $\sqrt{T/T_0}$ the linearity in relationship was observed (See Fig. 18).

The slope of the graph plotted was $\sqrt{3}$. This confirmed the validity of the stress equivalence and strain equivalence equations as stated in equations 38 and 47.

Isotropy Tests:

These tests were conducted at temperatures above and below the equicohesive temperature of 110°C and are shown in Figs. 19 to 21.

At 100°C test temperature, if it is assumed that by error, 0.4×10^{-3} of the instantaneous plastic strain had been included in the magnitude of the total creep strain for the specimen 'Z', then the three curves for the three specimens mutually at right angles are nearly coincident. It was noted that at this temperature where the total strain was small, the slight variation in applied stress, did not seriously affect the strain behaviour. If similar errors including some part of the instantaneous plastic strain, were corrected at 150°C , the specimens 'Z'₂ and 'X Y Z_{ag}' would show coincidence in creep behaviour. The stresses applied were in the proximity of 0.5 per cent proof stress where the material rapidly approaches the condition of uncontrolled plastic flow and hence the specimen 'XYZ'_{oe} showed considerable deviation from the previous two.

At 250°C - a temperature at which ageing and thermal softening produce considerable effects - six test specimens of the test stock axial direction were tested to select a reasonable stress level. Specimen 'Z'₃ at 12.10 tonf/in^2 showed a tertiary creep phase. Once again the stress level was slightly in excess of 0.5 per cent proof stress. At this level instability in plastic flow occurs rapidly. The variation in the creep behaviours of the specimens 'XZY'_{bf} and 'XZY'_{cd} - which are mutually at right angles - from that of the specimen 'Z'₃ are in line with the slight variations in the applied stresses.

The isotropic compatibility was confirmed. It was, however, noted that at 150° and 250°C if high stress levels were employed, the tertiary creep would occur earlier and this would lead to a scatter in the creep curve readings.

Preliminary Tests (Incremental Stress Tensile Creep)

The smaller solid section test specimens XIV and XV at 150°C were tested with early load stages of 200 and 100 hours durations each. This gives longer effective soak times for the successive stages and therefore for the full range of stress increments, lower stress levels for steady state creep were possible. In specimen XXX where the number of stages had been increased and in specimen XXXII where the first stress is higher, the 24 hour stage durations, reduced the effective soak times and therefore higher stress levels could be employed for the steady state creep region. This pattern of behaviour is in agreement with the effect of ageing and precipitation of the intermetallic compound Cu Al_2 . The process starves the matrix of strengthening particles and therefore accelerated creep rates occur.

Incremental stresses and the corresponding creep rates in each individual test show a good agreement with the exponential law which was stated by Dom²⁹ as applying to high stress levels and is shown in Fig. 22.

The deviation was at the early stages where the total creep strains were small and creep rate could not be assessed accurately. Similarly at the final load stages the creep rates were so high that the onset of the tertiary phase seriously affected the definition of the steady state creep rate. The scatter on the basis of the exponential law plot was marginal and was only apparent for the specimens XIV, XV and 5. In these specimens, either the effective soak times were large or the initial plastic strains were high.

At 200°C, the scatter between individual specimen results was noticed for specimens XIII, XVI and XVII in which the load stage durations were large but specimens XXIX and XXXII, which were at similar stress levels and had 24 hour load stage durations, showed coincidence and repeatability (Fig. 23). It was noticed that in long stage durations, when either the first stress was high or bigger stress increments were used, the instantaneous plastic strains and the creep strains were large. This led to the conclusion that at any instant in the secondary creep process, the rate at which work was absorbed was somehow related to the energy expended in distorting the material both elastically and permanently. The values $\sigma_{e_i} \dot{\epsilon}_{e_i}$ and $U_{\text{shear elastic}} + W_p$ (the plastic and previous creep work) were computed for these tests at 150°C and 200°C. These values plotted on a logarithmic scale in Figs. 25 and 26 revealed a relationship.

$$\sigma_{e_i} \dot{\epsilon}_{e_i} = M (U_{sh} + W_p)^N$$

where M and N were constants dependent upon the temperature. The deviation in the graphs for the last load stage indicated the onset of the tertiary phase of creep. It was also noted, that at the end of this continuity, the total energy of distortion in the different specimens tended to reach a limiting value. The scatter as compared with the exponential law was greatly reduced.

The tests at 250°C showed a reasonable agreement with the exponential law for individual specimens - see Fig. 24. Individually the graphs were displaced from each other and only coincided when the initial elastic shear energy and the initial plastic work were of similar magnitude. The results plotted on the basis of creep rate work and the energy of distortion reduced the relative displacement considerably as shown in Fig. 27.

The results of these tests analysed on the basis of the equations:

$$\dot{\epsilon}_c = A e^{B \sigma}$$

$$\text{and } \sigma_e, \dot{\epsilon}_e = M (U_{sh} + W_p)^N$$

are summarised below

$$\dot{\epsilon}_c = A e^{B \sigma}$$

Temperature °C	Stress Tonf/in ²	Creep Rate 10 ⁻⁶ /Hr	0.1% Proof Stress Tonf/in ²	$\left(\frac{B}{(\text{Tonf/in}^2)}\right)^{-1}$
150	18.8	5.0	21.3	1.25
200	12.4	5.0	17.1	4.20
250	8.0	5.0	10.8	4.20

$$\sigma_e, \dot{\epsilon}_e = M (U_{sh} + W_p)^N$$

Temperature °C	(minimum) in.tonf/in ³ /Hr x 10 ⁻⁶	$U_{sh} + W_p$ in.tonf/in ³ x 10 ⁻³	N
150	60.0	2.7	3.40
200	60.0	1.9	3.20
250	60.0	0.56	1.74

Main Test Programme (4 inch gauge length tubular specimens)

A series of incremental stress torsional creep tests were conducted at 150° and 250°C in order that equivalence with the tensile creep behaviour of one inch gauge length specimens could be established. This could not be distinctly established. Repeatability existed if the stress levels and the magnitude of stress increments were comparable. This was shown by specimens 'T20' and 'T21' at 250°C. The stress equivalence equation derived from the static tests as $\sigma_e = \sigma_x = \sqrt{3 \frac{T}{T_0}} \bar{\sigma}_{xy}$ was used. The simple incremental stress tensile creep test was repeated on the tubular specimen 'T28'. The deviation from the equivalent strain rate equation, also formulated earlier, was unacceptable. At this point, for combined tension-torsion loads, the incremental stress levels were optimised on the basis of the

equations 38 and 47.

$$\sigma_e = [\sigma_x^2 + (1.825 \tau_{xy})^2]^{1/2} \text{ at } 150^\circ\text{C}$$

$$\sigma_e = [\sigma_x^2 + (2.025 \tau_{xy})^2]^{1/2} \text{ at } 250^\circ\text{C}$$

The direct and shear components of the creep curves at 150° and 250°C are shown in Figs. 31 to 40 and 41 to 51 respectively. The data extracted are shown in tables XV and XVI. The mathematical analysis was based on the mathematical model detailed earlier. A similar analysis in the work of Stowell and Gregory³⁰ was also consulted. Their tests on an aluminium alloy⁶⁰⁶¹. T6 were conducted at 400°F .

In the mathematical model it was stated that in the steady creep phase, the strain rate components for the combined state were related by the equation 48.

$$\frac{\dot{\gamma}_{xy}}{\tau_{xy}} = 3 \frac{T}{T_0} \frac{\dot{\epsilon}_x}{\sigma_x}$$

For 150° and 250°C $\frac{\dot{\gamma}_{xy}}{\tau_{xy}}$ vs $\frac{\dot{\epsilon}_x}{\sigma_x}$ plot is shown in Figs. 49 and 50 respectively. The slopes of the linear relation are 3.31 and 4.09. These values confirm the validity of the proportionality factor $3 \frac{T}{T_0}$ for the two temperatures used.

The strain rate components and stress components were equated with the shear strain rate and the shear stress of an equivalent incremental stress torsional creep test. This was plotted in Figs. 51 and 52 as τ_e vs $\dot{\gamma}_e$. The agreement between the tests with different stress component ratios is good and emphasises that repeatability exists if the previous strain history is similar. The

σ_{ei} vs $\dot{\epsilon}_{ei}$ vs $(U_{sh} + W_p)$ plot in Fig. 53 shows a good agreement between the individual tests as well.

The creep tests when analysed according to the equations $\dot{\epsilon}_e = A e^{B\sigma_e}$ and $\tau_{ei} \dot{\epsilon}_{ei} = \sigma_{ei} \dot{\epsilon}_{ei} = M (U_{sh} + W_p)^N$ gave the following values:-

$$\dot{\epsilon}_e = A e^{B\sigma_e}$$

Temperature °C	$\dot{\epsilon}_e$ $10^{-6}/\text{Hr.}$	σ_e tonf/in^2	$\frac{B}{[\text{tonf/in}^2]^2}^{-1}$
150	5.0	18.6	1.02
250	5.0	8.45	2.47

$$\sigma_{ei} \dot{\epsilon}_{ei} = M [U_{sh} + W_p]^N$$

Temperature °C	$\sigma_{ei} \dot{\epsilon}_{ei}$ (minimum) $\text{in.tonf/in}^3/\text{Hr} \times 10^{-6}$	$[U_{sh} + W_p]$ $\text{in.tonf/in}^3 \times 10^{-2}$	N
150	60.0	2.60	3.27
250	60.0	0.50	1.64

At both the temperatures the behaviour in the incremental stress tensile creep tests exhibited greater deviation than the corresponding torsional creep behaviour. This behaviour is explained in the next section. For the same energy of distortion, the secondary creep work rate on tubular specimens was slightly lower than that of one inch gauge length specimens.

METALLURGICAL EXAMINATION, RELEVANT
SURVEYS AND DISCUSSION

The details and history of specimens which were used for micro-examination are as follows:

Material State	Position of micro-graph in test specimen	Test Temperature	Remarks
Fig. 54.			
(i) As supplied	-	Room Temperature	Magnification X55
(ii) Static tension to destruction	Near fracture section	"	1 in. gauge length specimen.
(iii) Static tension to destruction	$\frac{1}{4}$ in. away from fracture		Magnification x 905
Fig. 55.			
(i) As supplied	-	150°C	Soak period 118 hrs.
(ii) Tensile Creep incremental stress	Main section	150°C (Magnification 55)	1 in. gauge length total strain 2.1×10^{-2} .
(iii) - Ditto -	Main section	150°C (Magnification 905)	Total soak time 400 Hrs. (Effective)
(iv) Tensile Creep increment stress	Wall thickness	200°C (Magnification 55)	{ Tubular specimen 4 in. gauge length Total strain to fracture 4.0×10^{-2}. Soak period 100 Hrs.
(v) - Ditto -	Wall surface	200°C (Magnification 55)	
Fig. 56(a)			
(i) As supplied	-	250°C (Magnification 55)	Soak period 140 Hrs.
(ii) Tensile Creep increment stress	transverse cross section	250°C (Magnification 905)	1 in. gauge length Total effective soak period 170 Hrs. Total strain 0.85×10^{-2}
(iii) - Ditto -	Axial cross section. Near neck formation	250°C (Magnification 905)	

Material State	Position of micro-graph in test specimen	Test Temperature	Remarks
Fig. 56(a) (iv) Tensile Creep incremental stress	Axial cross section $\frac{1}{4}$ in. from neck formation	250°C (Magnification 905)	1 in. gauge length Total effective soak period 170 Hrs. Total strain 0.85×10^{-2}
Fig. 56(b) (i) Static torsion to rupture	Wall surface	250°C (Magnification 905)	Soak period 6 Hrs. 4 in. tubular specimen
(ii) Torsion Creep incremental stress	Wall thickness	250°C (Magnification 905)	Tubular specimen 4 in. gauge length Total shear strain 1.35×10^{-2} to collapse
(iii) - Ditto -	Wall surface	250°C (Magnification 905)	

The specimens were suitably cut and mounted in bakelite. They were polished on reducing micrograde silicon carbide papers and finish-polished with one micron diamond paste. They were given varying degrees of etch to ensure good definitions of grain boundaries, precipitates or cavities. The etching solution was made up of 100 c.c. water, 2.0 c.c. nitric acid and 5.0 c.c. of Hydrofluoric acid.

All micrographs showed the intermetallic particles CuAl_2 aggregating along the direction of forging. With higher temperatures and longer effective soak times the aggregated particles grew in size. They therefore created a boundary of weakness.

At room temperatures, 150°C and 250°C the grain sizes varied from 0.006in. transversely and 0.008in. axially with respect to the axis of forging. Precipitation started to occur at 150°C within the grains and intensified with increasing temperatures. At 250°C heavy precipitation was clearly visible in the grain matrix. This had allowed the grain more fluidity under stress. Under heavy direct strains cavitation occurred at grain boundaries associated with large particles of intermetallic compound.

Fig. 54 (i), (ii) and (iii)) shows that the intrinsic high strength of the alloy at room temperature leads to heavy slip at the grain boundaries. Extreme conditions of strain gave intercrystalline slip and fracture along these boundaries.

Fig. 55 (i) and (v) exhibit the onset of precipitate at 150°C and cavitation in the wall surface of tubular specimens at 200°C . The cavities acted as stress raisers and were associated with the onset of tertiary creep and the sudden collapse encountered in the tubular specimen tests.

Two features are highlighted by Figs. 56(a) and (b). Firstly, owing to high temperature (250°C) the material tended to recrystallise. Some of the grain boundaries which run between two parallel rows of intermetallic particles started to disappear. Secondly, heavy cavitation was observed in tensile creep tests but it was relatively low in torsional creep tests. The crack initiation in torsional creep can be seen in Fig. 56.b. (iii).

These findings throw some light on the conclusions suggested by other workers. Hence the mathematical relationship and the hypothetical equations proposed are not based on any previous work available.

Finnie³¹ states that in short term tests at 250°C for aluminium alloys the criterion of Von Mises is not borne out. He suggested that hydrostatic stresses have a significant effect on creep behaviour. He supports this statement by reference to the work of Read³².

Johnson³³ et al suggest that high strains leading to fracture is a phenomenon associated with crack propagation followed by rupture. They propose a principal stress biased criterion for equivalence. Many other workers have proposed the second invariant of the stress quadratic as the flow criterion. This presupposes that the material is not altered by environmental and prestraining effects. Capriz³⁴ proposes and assumes such behaviour.

Namestinkov³⁵ for combined stress creep under changing load conditions assumes the criterion of Von Mises and classical plasticity and suggests that the strain rate components follow the relation:

$$\frac{\dot{\gamma}}{\dot{\epsilon}} = \frac{3\tau}{\sigma} = 3\lambda$$

He states that the stress and strain vectors are coincident, but assumes that volumetric strain is significant and allows for this by altering Poisson's ratio from 0.5 to 0.4.

Rabotnov³⁶, however, states the equation of state for creep in terms of the parameters σ , T and q where 'q' characterises the structural state. Hence he proposes that

$$\dot{\epsilon} = C e^{\alpha\sigma + \beta T} \quad \text{where } \alpha \text{ and } \beta \text{ are constants.}$$

Kennedy³⁷ indicates increased dislocation mobility in short term creep behaviour. This allows the escape of solute atoms from dislocations and higher creep rates are encountered owing to previous strain. (This suggests that mobility is a potential or energy stored characteristic).

Pollard³⁸ and Nutting for a copper-aluminium alloy, containing 4 per cent copper by weight, have shown that above a critical stress level, the onset of steady state creep gives a stress independent, constant dislocation density, hence:

$$\dot{\epsilon} = A_1 \sigma^n$$

They observed shearing in the grain substructure and stated that at 250°C for 100 hours a steady creep rate was 10 times greater than that at 200°C for the same stress. The reason is that the particles of intermetallic compound are no longer effective barriers at the grain boundaries. With the removal of hardening particles, the region near the grain boundary became relatively weak and consequently more deformation was possible. The change from particles in the matrix to the precipitate Cu Al₂ phase, produced volumetric contraction (approximately 2 per cent linear contraction). Recovery in the secondary creep phase was limited. These conclusions consolidate the views expressed in the current programme and justify the modification made to the Von Mises criterion. Work by Cottrell³⁹ and Sully and Hardy⁴⁰ strengthens the hypotheses used in the analysis of results.

Fell and Doyle⁴¹ have shown that in the RR58 alloy in sheet form, dislocations occur along the particles of intermetallic compounds and grain boundaries. These are also formed by condensation of thermal condensates. Precipitates at temperatures in excess of 190°C are heavy. This will lead to large creep rates.

The scatter, between the creep results of one inch gauge length and 4 inch gauge length specimens is explained by the aspects revealed by Dodds and Davies⁴². They state that the ratio between the grain boundary sliding and total creep elongation is not constant throughout the specimen cross section. As surface grains are less restricted, a greater amount of boundary sliding occurs in them.

In the solid specimens used in the present work, percentage cross section for grain boundary slip is less than that for the tubular specimens. Hence these solid specimens give higher stress values for the same strains and strain rates.

If voids and cavities develop, the small solid cross section is more stable than the tubular section in which the same effect produces marked strength variations along the periphery. Such an occurrence produces smaller strain magnitude in the tubular specimen upto the onset of the tertiary phase of creep.

DISCUSSION

In order to predict creep behaviour, under complex and incremental stress conditions at elevated temperatures, three things are essential:-

- (1) A suitable test machine and appropriate instrumentation for strain measurements.
- (2) Accurate temperature and stress control.
- (3) A viable hypothesis which takes into account the test material behaviour that results from subsidiary microstructural changes.

Complex Creep Machine

A test machine for complex stress tests should be capable of applying direct and shear stresses in all the three spatial dimensions of a test specimen. These are six independently variable stresses; three of which are direct stresses while the remaining three are complementary shear stresses. Most materials have a certain amount of ductile behaviour within the working stress ranges and therefore are relatively unaffected by hydrostatic stress. Hence most combinations of the deviatoric components of principal stresses can be obtained by making one of the principal stresses zero while the other two are varied. Hsu⁴³ has conducted a comprehensive theoretical analysis of such machines and the machine designed in the present work conforms to all the requirements named.

The temperature and stress control has already been discussed in the main text and the appendix.

Test Material Selection

Aluminium RR58 to the specification DTD 731 was selected in preference to Aluminium RR59. Little or no work has been done on the material selected under incremental stress at elevated temperatures although it is at present being used in the new generation of supersonic aircraft.

This material is also more creep resistant. For example, at room temperature for steady state creep the working stress range for RR58 is 23.0 tonf/in² to 26.0 tonf/in² while that for RR59 is 20.5 tonf/in² to 23.0 tonf/in². With the slightly larger stress range it was found that large load increments, in the incremental stress test, could be used. The increments also gave a larger variation in creep rates than those observed for the material RR59.

Preliminary Static Tests

Static tensile tests carried out at elevated temperatures show reasonable agreement up to 150°C with the data supplied by High Duty Alloys Ltd. The slight scatter is less than the 5 per cent variation in mechanical properties which is generally accepted for copper-aluminium alloys of which RR58 is one. The deviation, from the acceptable scatter, is due to the variations in the microstructure. It depends upon the dispersion of the hardening particles, the solubility variation in the material matrix and other structural defects. These variations are due to the complexity of the structure of aluminium alloys. The behaviour of the complex microstructure is not even fully understood by metallurgical investigators.

The scatter above 150°C in the tensile data is magnified owing to the inherent structural complexity and its effect on strain behaviour. The complete range of tests up to 300°C shows variation from the data made available by the manufacturers and is unique for the test material stock used. No torsional data

were available while such data were useful in formulating approximate equations of stress and strain equivalence.

The tensile data supplied show that the mechanical strength deteriorates with increasing soak times and higher temperatures. Torsion static tests with test histories similar to those of the tensile tests, soak times, temperatures and prior strain - were carried out so that an inter-relation between them could be investigated.

Most non-ferrous metals are reputed to follow the shear strain energy criterion for yield and plastic flow behaviour. If such a behaviour exists then for simple tension and simple torsion the relationship between the stresses is

$$\sigma_x = \left[3 \tau_{xy}^2 \right]^{\frac{1}{2}} \quad \begin{array}{l} \sigma_x = \text{stress in simple tension} \\ \tau_{xy} = \text{stress in simple torsion} \end{array}$$

and on the work hypothesis, the corresponding strain equivalence between simple tension and simple torsion is

$$\epsilon_x = \frac{\gamma_{xy}}{(3)^{\frac{1}{2}}}$$

0.1, 0.2 and 0.5 per cent proof stresses and the corresponding shear stresses derived from these tests on the equivalent strain basis confirm the shear strain energy criterion up to 100°C only as shown in Table VII(a). Above this temperature when stress and strain equivalence equations (38) and (47) are used the plot $\frac{\sigma_x}{\tau_{xy}}$ vs $\left(\frac{\tau}{\tau_0} \right)^{\frac{1}{2}}$ gives a slope of $(3)^{\frac{1}{2}}$ which confirms their validity. The sudden change in the stress vs temperature plot in Figs. 19 and 20 shows that around 200°C the co-agulation of precipitates leaves the matrix sufficiently soft to produce rapidly increasing subsequent strains.

These tests also revealed the magnitude of strains which are likely to occur and also served as a guide to the magnitude of the stresses which were used in the creep tests to follow.

Isotropic Compatibility

The simple creep tests, at 100° , 150° and 250°C on test specimens orientated in various directions from the test stock axis, have shown considerable agreement. At 100°C , the creep strains were small. The discontinuity observed in the creep curves is due to the high sensitivity of the instrumentation. It is also difficult, under such conditions, to define the end of the instantaneous plastic flow and therefore the inadvertent inclusion of some plastic strain for specimen 'Z' shows lack of coincidence with the creep curves for the other specimens.

Tests at 150° and 250°C were conducted at high stress levels so that larger creep strains could be obtained than those at 100°C . The stress levels employed are approximately 0.5 per cent proof stress. Dodds⁴² and Davies have shown that under such conditions serious grain boundary sliding occurs and slight changes in stress levels also produce significant changes in creep strain behaviour. The variation in strains observed are in line with the variation in the stresses applied. Hence the isotropic compatibility is confirmed and it is to be noted that at elevated temperatures where microstructural changes are accelerated, a variation in the initial homogeneity of the material will produce some deviation between individual specimens for the same stress level.

Preliminary Incremental Stress Tensile Creep Tests

It was essential to choose and establish the stress levels for a multi-load stage creep test. The two objectives were, firstly that the stress increment be significant and secondly, that the creep strains for the stress increment be sufficiently

varied, since the test material exhibits a controlled plastic strain range of 1.25 to 1.50 per cent only. It was also intended that from the data obtained, the validity of one of the existing creep laws should be confirmed.

Early in this programme ~~of tests~~ it was discovered that primary creep in this material at the temperatures used is insignificant at the stresses employed. The tertiary phase was indecipherable. This behaviour has been confirmed by the corresponding torsional creep tests which are listed in the main test programme. Secondary creep therefore became the sole field of investigation. At all temperatures, at the stress levels used the creep tests follow the law

$$\dot{\epsilon}_c \propto e^{B\sigma} \text{ or } \dot{\epsilon}_c = A e^{B\sigma}$$

where A and B are constants for any particular temperature. The magnitudes of the stresses applied were dictated by the need for using a 24 hour duration for each load stage. (This choice of duration was critical because a longer stage duration would have made the total experimental work lengthy and outside the possible span of an academic investigation). Secondary creep law follows the law enunciated. The critical minimum stress for the temperatures used and the corresponding creep rates are:-

Temperature °C	Stress ($\bar{\sigma}_x$) tonf/in ²	Secondary Creep Rate ($\dot{\epsilon}_c$) 10 ⁻⁶ /Hr
150	18.80	5.0
200	12.25	5.0
250	8.00	5.0

The scatter between the results of different test specimens is caused by longer stage duration or high initial stress or stress increments. At 150°C, stresses below 18.4 tonf/in² or above 21.4 tonf/in² give different creep laws.

At 200° and 250°C serious precipitation occurs. The precipitate aggregates and also appears co-agulated throughout the grain structure. Each test specimen still obeyed the exponential creep law under incremental stress stages. Precipitation within the matrix softens^{it} while any increase in the intensity of the first stage load only displaces the creep curve. The scatter between individual test specimens where the $\dot{\epsilon}_c \propto e^{\sigma}$ relation is plotted, indicates that the creep rate at the stress applied, which can be expressed as secondary creep work rate, i.e. $\sigma_{e_i} \cdot \dot{\epsilon}_{e_i}$ is related to the material state at that particular instant. The material state is commonly defined by the previous strain history while the plastic flow is characterised by the total shear strain energy. This leads to the hypothesis postulated that the secondary creep work rate depends upon the stored energy of distortion.

Main Test Programme

The metallurgical examination has revealed that the creep rates depend upon the applied stress, the resulting strain and the precipitation which occurs. At 150° and 250°C the incremental stress creep, for torsional loads in which five load stages, each one of 24 hours duration were used, gave the following stress ranges:

Temperature °C	Shear stress range Tonf/in ²
150	10.20 to 11.60
250	4.35 to 4.90

Stresses below the lower limit produce extremely low creep rates. In some of the preliminary tests which are not listed, the creep rates became significant only during the prolonged stage durations of 200 hours and were therefore attributed to over-ageing and thermal softening. The stresses in excess of the upper limit soon produced the onset of the tertiary phase and sudden collapse. One inch gauge

length specimens, in the preliminary incremental stress tensile creep, were very sensitive to environmental disturbance and vibrations, whereas similar tests in the complex stress machine with slightly lower stresses did not exhibit such sensitivity. This shows that the environmental disturbances are dampened within the loading system of the complex creep testing machine.

The stress equivalence and strain rate equivalence based on the static tests, were applied to these combined tension-torsion results. The analysis is recorded in table XIX and plotted in Figs. 49 and 50. The linearity in the relationship of the following equations is therefore confirmed.

$$\sigma_e = \left[\sigma_x^2 + 3 \frac{T}{T_0} \tau_{xy}^2 \right]^{\frac{1}{2}} \quad - \quad \text{stress equivalence}$$

$$\dot{\epsilon}_e = \left[\dot{\epsilon}_x^2 + \frac{\dot{\gamma}_{xy}^2}{3 T/T_0} \right] \quad - \quad \text{strain rate equivalence}$$

This reinforces the modification, deemed necessary, to the criterion of Von Mises. The secondary creep rate behaviour within the working stress range is energy dependent as postulated by Dorn and leads to an equation of the form

$$\dot{\epsilon} \propto e^{B\sigma}$$

The scatter, in the torsional creep data and the tensile creep results at high stresses, shows that the effects of environmental disturbance, however, small, are critical. In the combined tension-torsion load creep, the simultaneous dampening effect of the loading configurations owing to environmental disturbances, produces little scatter in the strain results.

If the creep behaviour, at the stresses employed, is treated as a phenomenon in which the capacity to absorb work is some function of the energy already absorbed by virtue of its elastic and permanently distorted state, the results have to be related by the equation of the hypothesis which was stated as:-

$$\sigma_{e_i} \dot{\epsilon}_{e_i} = M \left[U_{sh} + W_p \right]^N$$

The comparison between simple tensile incremental stress creep and combined load creep shows the following agreement.

Temperature °C	One inch gauge length solid specimens			Four inch gauge length tubular specimens		
	σ_{ei} $\dot{\epsilon}_{ei}$ in tonf/in ³ /Hr $\times 10^{-5}$	$U_{sh} + W_p$ in tonf/in ³ 10^{-2}	N	σ_{ei} $\dot{\epsilon}_{ei}$ in tonf/in ³ /Hr $\times 10^{-6}$	$U_{sh} + W_p$ in tonf/in ³ $\times 10^{-2}$	N
150	60.0	2.70	3.40	60.0	2.60	3.27
200	60.0	1.90	3.20	-	-	-
250	60.0	0.56	1.64	60.0	0.50	1.64

The agreement between the two types of specimens under different types of loads is good. The higher energy of distortion and the slightly larger 'N' value, shows that the comments made in the section headed "Metallurgical examination" are valid. The smaller free surface in the solid section specimens makes them more stable against voids and cavities and therefore creep strains can take place along longer paths.

CONCLUSIONS AND SUGGESTIONS FOR FURTHER WORK

The original intention was to produce an experimentally backed theory for complex and incremental stress creep at elevated temperatures. No relevant creep data were available nor had any extensive work been done on the H14 DTD 731 aluminium alloy under the test conditions used in this research. It is a practical engineering material which has been developed for medium strength to operate especially at elevated temperatures approaching 250°C.

The tests conducted show that there are two special characteristics under creep conditions:

(i) The material has an equicohesive temperature just above 100°C. At this temperature some precipitation and ageing occur so that the strength of the grain boundaries become the same as that of the grains themselves. When temperatures higher than 200°C are encountered, precipitation occurs in two forms. Firstly, the precipitate aggregates along the grain boundaries, the grains grow in size and the remaining precipitate co-agulates within the grain substructure. These metallurgical changes have shown greater fluidity in the direction of the applied shear.

(ii) At low stress levels most of the strain is due to the primary creep component. The secondary creep rates are essentially small and approach a maximum of 15×10^{-6} /hour. Beyond this region and up to 0.2 per cent creep strains, steady state creep follows the power law of the form

$$\dot{\epsilon} = A_1 \sigma^n \quad \text{where } A_1 \text{ and } n \text{ are constants for the given temperatures}$$

The main test programme was designed for incremental stress conditions. The stress levels necessary to obtain significant creep strains, are found to be high. When these are applied, the primary creep is insignificant while the onset of the

tertiary phase has led to sudden collapse of the tubular specimens used. Hence the entire analysis relates only to steady state creep behaviour. For the working stress range in the tests, the creep is energy dependent and therefore follows the law.

$$\dot{\epsilon}_c = A e^{B\sigma} \quad \text{where A and B are constants for the given temperature}$$

Beyond 1.25 to 1.50 per cent permanent strain, the creep behaviour alters and is predominantly tertiary. The sudden collapse of tubular specimens at the onset of the tertiary phase is apparently caused by cavitation. This can be seen in the micrographs in which the regions of fracture show heavy cavitation. The actual duration of the tertiary state creep before the crack propagation and sudden collapse is so short that sufficiently accurate readings cannot be obtained. Hence a viable theory for the tertiary creep behaviour under incremental stresses cannot be formulated.

An attempt was made to correlate the creep rates at the various load stages for incremental stress tests. Since neither the strain hardening nor the time hardening relationships gave an acceptable agreement, the attempt was abandoned. When, however, the exponential law for steady state creep was applied to both simple tension as well as the combined tension torsion incremental stress results, the good agreement obtained, confirmed that creep is energy-dependent. Secondly it also confirmed that, at the temperatures used, the effects of time and strain hardening are obliterated by the microstructural changes which are caused by thermal softening, grain growth and polygonization. The values of the constants A and B for the same temperature, derived from one inch gauge length specimen tests show that creep is sensitive to specimen size and environmental disturbance. Tubular specimen test results which were derived by using various combinations of tension and torsion loads give good repeatability and the constants A and B derived are therefore more reliable.

For the minimum creep rates quoted against corresponding stresses the values A and B are 3.0×10^{-14} per hour and $1.02 \text{ in}^2/\text{tonf}$ at 150°C and 4.4×10^{-15} per hour and $2.47 \text{ in}^2/\text{tonf}$ at 250°C . These values are unique for the material and can only be used in predicting creep behaviour for the stress range employed.

The equations of equivalence, as confirmed by the tension-torsion tests, are

$$\sigma_e = \left[\sigma_x^2 + 3 \frac{T}{T_0} \tau_{xy}^2 \right]^{\frac{1}{2}}$$

$$\dot{\epsilon}_e = \left[\dot{\epsilon}_x^2 + \dot{\gamma}_{xy}^2 / 3 \frac{T}{T_0} \right]^{\frac{1}{2}}$$

These equations prove the strain vector is normal to the current yield surface and therefore no anisotropy develops. Even though the coefficient, 3, in the equations of equivalence derived from the criterion of Von Mises has been magnified, no anisotropy developed to account for this magnification.

The exact behaviour established is unique for incremental stress steady state creep when the increment stress stage has a 24 hour duration. If these equations are to be applied in cases where the stage duration is longer, the coefficients will have to be checked experimentally.

It was observed that the creep behaviour, between the tests of long stage duration and those using higher initial stresses, was similar. The investigation of the interrelation between the total energy of distortion and the subsequent steady creep work rate followed the form

$$\sigma_{ei} \dot{\epsilon}_{ei} = M \left[U_{sh} + W_p \right]^N$$

For the same temperature, the displacement between the incremental stress tensile creep results and the similar tension-torsion results when plotted on this basis showed much smaller deviation than that for the exponential law plots. The values of N, minimum $\sigma_{ei} \dot{\epsilon}_{ei}$ and corresponding $[U_{sh} + W_p]$, derived earlier, show very close agreement between the two sets of tests.

If for creep behaviour the relationships established and the equations of equivalence proposed are to be applied universally, future researchers must:

- I (i) Conduct similar tests for the test material at 100° , 125° , 175° and 200°C .
- (ii) Repeat the tests above but reverse the applied shear at each successive load. This may show some effects which are caused by significant rotation of the principal axes. If the pattern of results is different from that in (i) then some additional modifying factors may have to be included.
- (iii) Investigate similar materials and other alloys which also exhibit similar metallurgical changes at elevated temperatures, e.g. Magnox and steels.
- II (i) Investigate the behaviour, with decreasing stress levels, in the test material and the materials mentioned in I. The stress decrements should be so chosen that the total creep strain does not exceed the total creep strain encountered in the present tests.
- (ii) Repeat decreasing stress level tests on the same pattern as the tests in I(i) and I(ii).
- (iii) Apply the above conditions to tests under biaxial tension for the same stress levels. The stress components can be varied proportionately or at random.

Once the creep behaviour has been uniquely defined for increasing and decreasing stress levels, any future development should lead to intermittent stress variations in experimental programmes. Subsequently, similar programmes can be formulated, in which the temperature may be varied at random or at predetermined intervals.

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APPENDIX 1.

Instrumentation and Specimen Details

Figs. 6 - 8

1.00" GAUGE LENGTH SPECIMEN
FOR USE IN
3/4 TON DENISON TENSILE CREEP TESTER.

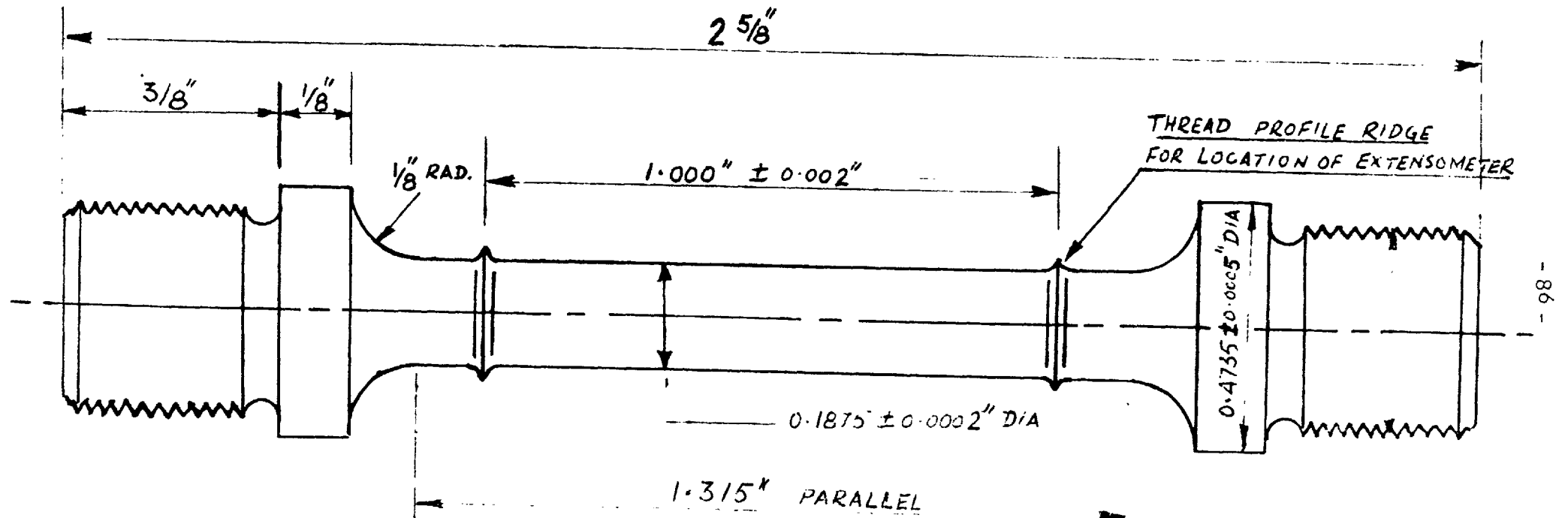
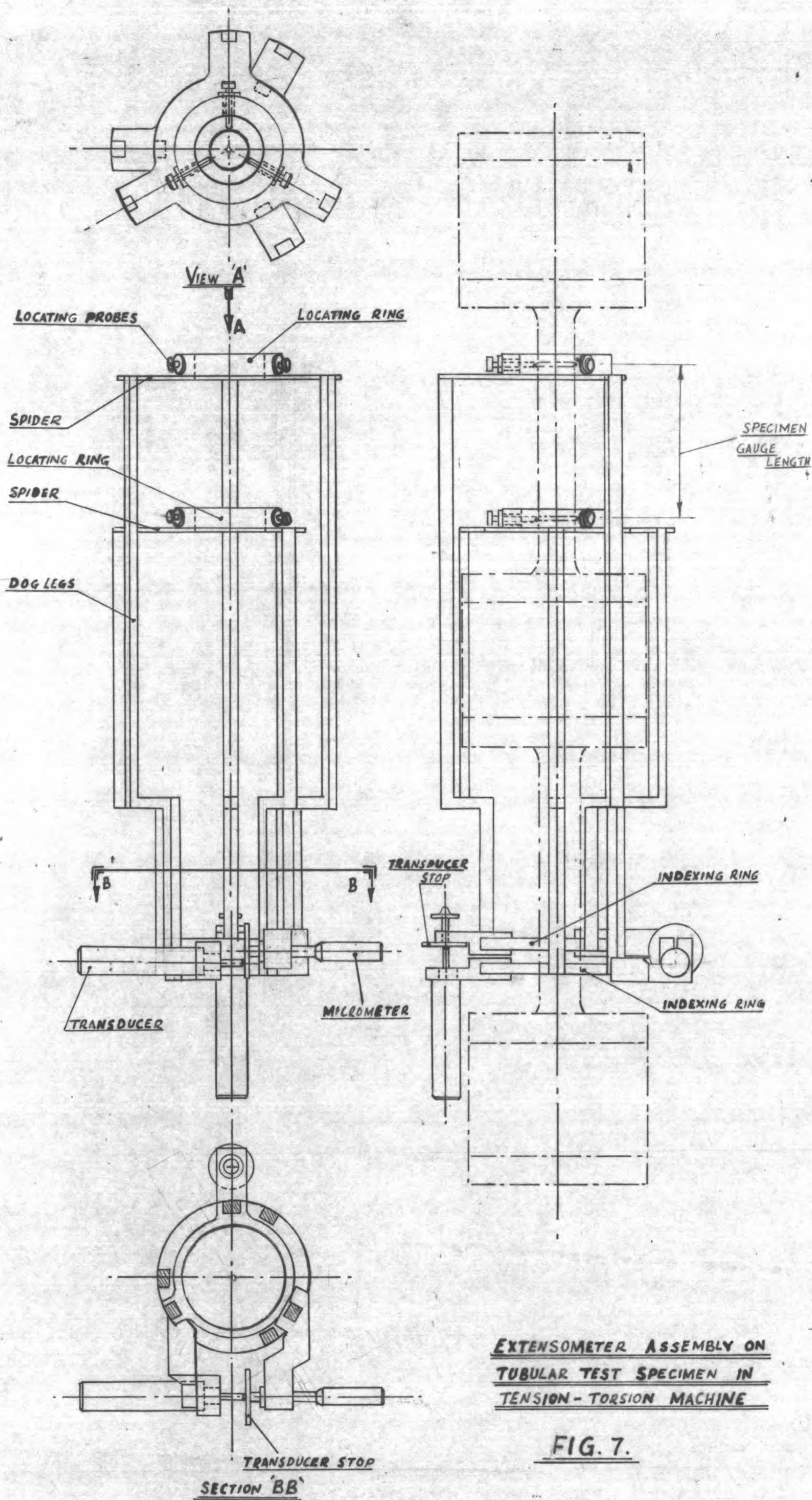


FIG. 6. SCALE: 4x FULL SIZE $3/8"$ BSE THREAD



EXTENSOMETER ASSEMBLY ON
TUBULAR TEST SPECIMEN IN
TENSION - TORSION MACHINE

FIG. 7.

TEMPERATURE DISTRIBUTION ALONG
DUMMY SPECIMEN WITH EXTENSOMETER NO. 2
(MODIFIED) IN POSITION AS ASSEMBLED IN
THE COMPLEX CREEP MACHINE.

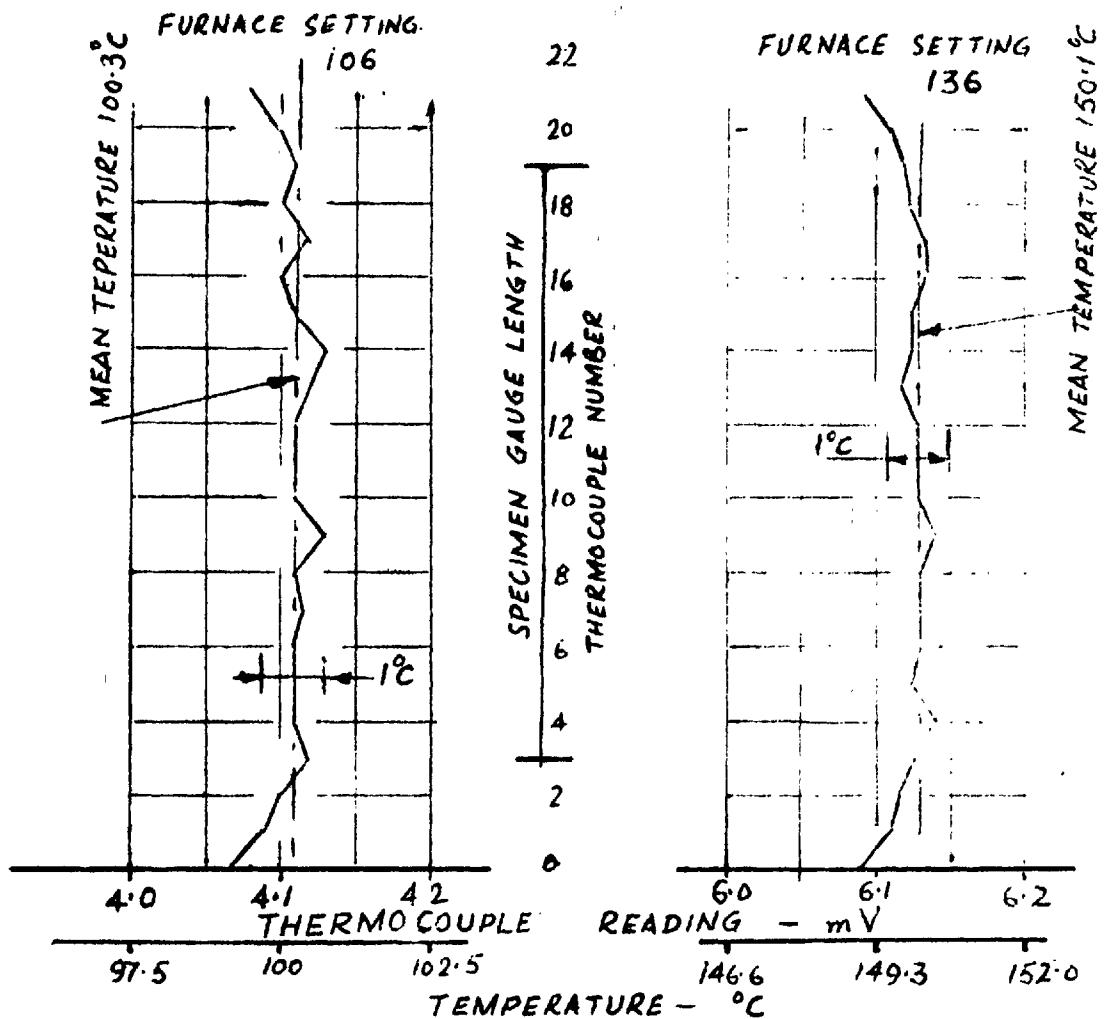


FIG. 8

APPENDIX 2

Experimental Data, calculated Results and Curves

Tables 1 - XIX

Figs. 9 - 57

TABLE I

Test Material Selection

-

Static Tensile Tests (See Fig. 9)

SPECIMEN (A) RR.59 ○

Outer Dia. = 1.098 in.

Inner Dia. = 1.00 in.

Gauge Length = 4.000 in.

Cross Sectional Area = 0.1615 in²

$E = 10.8 \times 10^{+6} \text{ lbf/in}^2$

SPECIMEN (B) RR.58 ×

Outer Dia. = 1.100 in.

Inner Dia. = 1.000 in.

Gauge length = 4.000 in.

Cross sectional Area = 0.165 in²

$E = 10.8 \times 10^{+6} \text{ lbf/in}^2$

Tensile Stress Tonf/in ²	Ext ⁿ ×10 ⁻³ in	Total Strain % × 10 ⁻¹	Tensile Stress Tonf/in ²	Ext ⁿ ×10 ⁻³ in	Total Strain % × 10 ⁻¹
○	○	○	○	○	○
1.312	0.93	0.232	1.285	1.02	0.255
2.625	1.97	0.492	2.570	2.04	0.510
3.937	3.03	0.758	3.855	3.06	0.765
5.250	4.10	1.025	5.140	4.10	1.025
6.562	5.22	1.300	6.425	5.16	1.290
7.875	6.30	1.575	7.710	6.22	1.555
9.187	7.28	1.820	8.995	7.26	1.815
10.500	8.40	2.100	10.280	8.34	2.085
13.125	10.30	2.575	12.850	10.70	2.675
18.375	20.80	5.200	17.990	15.70	3.925
21.000	38.20	9.550	-	-	-
22.312	53.1	13.275	-	-	-
22.837	71.1	17.750	-	-	-
23.100	73.3	18.325	23.130	29.6	7.400
23.890	95.1	23.775	24.415	40.2	10.050
26.40 Stress to fracture			25.186	55.7	13.925
			25.700	67.8	16.950
			26.214	80.6	20.150
			26.728	98.6	24.650
			28.78 Stress to fracture		

TABLE II

Test Material Selection

$E = 10.7 \times 10^6 \text{ lbf/in}^2$
RR.59 SPECIMEN (C) ϕ

Tensile Creep Tests (see Figs. 10 and 11)

$E = 10.8 \times 10^6 \text{ lbf/in}^2$
RR.58 SPECIMEN (D) $\boxtimes$

Stress	Duration	Steady Creep Rate	Total Creep Strain	Stress	Duration	Steady Creep Rate	Total Creep Strain
Tonf/in ²	Hours	in/in/hr	$\times 10^{-3}$	Tonf/in ²	Hours	in/in/hr	$\times 10^{-3}$
21.0	164	6.3×10^{-6}	1.35	-	-	-	-
23.1	1	545×10^{-6}	1.80	23.13	167	0.7×10^{-6}	0.2
23.89	1½	950×10^{-6}	6.83	24.672	1	82.5×10^{-6}	0.18
				25.70	35/60	335×10^{-6}	0.55
				26.73	40/60	1120×10^{-6}	1.73

TABLE III

Elevated Temperature Tensile on HIDUMINIUM RR58 forged Bar DTD 731.

Fully heat treated

Test Temp. °C	Soak Period Hours	Proof Stress Tonf/in ²		Tensile strength tonf/in ²	Elevation % on $4\sqrt{A}$
		.1%	.2%		
Room Temp. -		22.8	24.2	29.5	9
		*Selection Test 22.0	23.6	28.8	Tubular Spec.
100	1	22.1	23.3	28.3	10
	10	22.6	23.9	28.6	7
	100	22.1	23.1	27.4	10
150	1	21.1	22.3	26.3	14
	10	21.3	22.3	26.2	12
	100	20.6	21.6	24.8	15
200	1	19.3	20.3	22.9	17
	10	17.1	17.7	19.6	18
	100	13.6	14.3	16.0	21
250	1	14.0	14.6	16.2	22
	10	10.8	11.2	13.3	25
	100	7.2	7.8	10.8	31
300	1	9.8	10.5	12.2	25
	10	5.0	5.5	7.7	28
	100	2.1	2.4	5.8	49
350	1	5.2	5.7	7.0	28
	10	2.1	2.3	3.8	90
	100	1.4	1.5	2.9	106

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TABLE IV

Creep Tests of HIDUMINIUM RR58 Forged bar DTD 731

Temp. °C	Youngs Modulus x10 ⁶ lbf/in ²	Total % Plastic Strain	Estimated Stress (Tonf/in ²) to produce							
			Specified amount of strain Hrs					Rupture		
			10	100	300	500	1000	100	100	5000
100	10.2	0.1	20.9	20.5	20.3	20.2	19.9			
		0.2	21.4	21.2	21.1	21.0	20.9	24.0	22.0	(20.4)
		0.5	22.0	21.8	21.7	21.7	21.6			
150	9.6	0.1	17.7	16.4	15.9	15.5	14.8			
		0.2	18.8	17.1	16.6	16.4	16.1	18.4	16.2	(15.0)
		0.5	19.9	17.8	17.2	16.8	16.5			
200	9.3	0.1	14.0	10.8	9.4	8.7	8.0			
		0.2	15.0	12.3	11.0	10.2	9.5	13.8	10.6	(8.7)
		0.5	15.9	13.2	11.8	11.1	10.5			
250	8.8	0.1	8.0	5.5	4.3	3.9	3.4			
		0.2	9.0	6.5	5.2	4.6	4.1	8.2	5.2	(3.0)
		0.5	10.0	7.3	6.1	5.5	4.7			
300	8.2	0.1	3.5	2.1	1.3	1.0	0.7			
		0.2	3.8	2.4	1.7	1.3	1.0	2.8	1.6	(1.0)
		0.5	4.0	2.5	1.9	1.6	1.3			
350	-	0.1	1.1	0.6	0.4	0.3	0.2			
		0.2	1.2	0.8	0.6	0.5	0.4	1.0	0.6	(0.3)
		0.5	1.3	0.9	0.7	0.6	0.5			

Stresses estimated to produce rupture in 5000 Hrs have been extrapolated

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Slough, Bucks.

TABLE V

Tensile Properties Derived at Elevated Temperatures (see Figs. 12, 13 and 14)

HIDUMINIUM RR58 DTD 731

Specimen Code	Temperature	Young's Modulus 10^6 lbf/in^2	Proof Stress Tonf/in^2		
	$^{\circ}\text{C}$		0.1%	0.2%	0.5%
①	Room Temp 19.2	10.6	24.0	25.4	26.8
②	57	10.2	23.4	24.8	26.1
③	101	10.1	22.9	23.9	24.6
④	125	9.85	22.4	23.3	24.3
⑤	151	9.55	22.1	22.8	23.4
⑥	161	9.45	21.6	22.1	22.6
⑦	172	9.20	19.9	20.5	21.2
⑧	192	9.10	18.7	19.5	19.9
⑨	214	8.95	15.9	16.5	17.0
⑩	237	8.90	14.7	15.1	-
⑪	274	8.35	11.2	11.6	-

TABLE VI

Torsional Properties Derived at Elevated Temperatures (see Figs. 15, 16 and 17)

HIDUMINIUM RR58 DTD 731

Specimen Code	Temperature °C	Modulus of Rigidity 10 ⁶ lbf/in ²	Shear Stress in Tonf/in ² for permanent shear strains		
			0.1 %	0.2 %	0.5 %
T1 (1)'	Room Temp. 20.4	4.12	13.4	14.5	15.1
T2 (2)'	62.5	4.00	13.2	14.2	14.7
T3 (3)'	117	4.00	12.4	12.9	13.6
T4 (4)'	161	3.75	10.5	11.35	12.35
T5 (5)'	179	3.62	9.8	10.70	11.30
T6 (6)'	203	3.55	8.2	8.6	8.85
T7 (7)'	228	3.48	7.2	7.5	7.8
T8 (8)'	255	3.42	6.0	6.4	6.7
T9 (9)'	275	3.33	5.15	5.50	5.7
T10 (10)'	301	3.25	4.2	4.40	4.65

TABLE VII(a)

Equivalent Stress-Strain Data

HIDUMINIUM RR58 DTD 731

(Derived from the tensile and torsion static tests)

Temperature Range - Room temperature to 100°C.

Temp.	Proof Stress σ_x , in Tonf/in ²			Shear Stress, τ_{xy} , in Tonf/in ² for permanent shear strains		
°C	(A) 0.1%	(B) 0.2%	(C) 0.5%	a 0.173%	b 0.346%	c 0.865%
Room Temp	24.0	25.4	26.8	14.1	14.7	15.4
50	23.6	24.8	26.3	13.8	14.4	15.2
75	23.3	24.4	25.6	13.6	14.1	14.8
100	22.9	23.9	25.0	13.3	13.8	14.5

Temp °C	$\frac{\sigma_x}{\tau_{xy}}$ A/a	(Equivalent Stress Ratio) B/b	C/c	Average $\frac{\sigma_x}{\tau_{xy}}$
Room Temp	1.705	1.730	1.740	1.725
50	1.710	1.725	1.730	1.720
75	1.715	1.730	1.730	1.725
100	1.725	1.735	1.725	1.730

TABLE VII(b)

Equivalent Stress-Strain Data (see Fig.18)

HIDUMINIUM RR58 DTD 731 (derived from tensile and torsion static tests)

At Elevated Temperatures

T = Test temperature °K

T₀ = Equicohesive temperature °K = 273 + 110 = 383

* = Proof Stress (Shear) are for (0.1%, 0.2% and 0.5%) x

$\sqrt[3]{T/T_0}^{\frac{1}{2}}$ Shear Strain

Temp °C	$\sqrt[3]{T/T_0}^{\frac{1}{2}}$	Proof Stress Direct σ_x Tonf/in			*Proof Stress Shear σ_{xy} Tonf/in		
		0.1	0.2	0.5	$0.1\sqrt[3]{T/T_0}^{\frac{1}{2}}$	$0.2\sqrt[3]{T/T_0}^{\frac{1}{2}}$	$0.5\sqrt[3]{T/T_0}^{\frac{1}{2}}$
		A	B	C	a	b	c
125	1.020	22.4	23.3	24.3	12.7	13.2	13.8
150	1.051	22.2	22.8	23.4	12.1	12.5	12.9
175	1.081	19.9	20.6	21.1	10.6	11.0	11.25
200	1.111	16.6	16.9	17.2	8.5	8.75	8.90
225	1.140	14.9	15.3	15.6	7.5	7.75	7.90
250	1.169	12.9	13.4	13.7	6.4	6.65	6.80
275	1.197	11.1	11.6	-	5.35	5.60	-
300	1.224	9.3	9.7	-	4.4	4.6	-

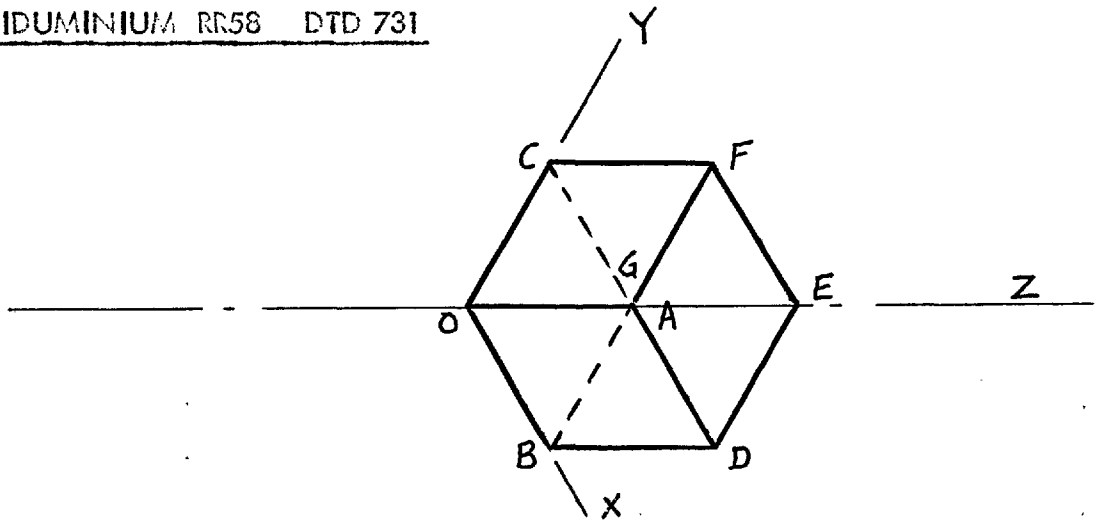
Temp °C	T ₀ K	A/a	B/b	C/c	Average $\frac{\sigma_x}{\sigma_{xy}}$
125	398	1.765	1.76	1.76	1.76
150	423	1.83	1.83	1.815	1.825
175	448	1.87	1.87	1.87	1.87
200	473	1.93	1.93	1.935	1.94
225	498	1.985	1.975	1.975	1.98
250	523	2.02	2.02	2.02	2.02
275	548	2.07	2.06	-	2.07
300	573	2.11	2.10	-	2.11

TABLE VIII

Isotropy Tests (see Figs. 19, 20 and 21)

Constant Stress Tensile Creep

HIDUMINIUM RR58 DTD 731



Test Temp	Specimen Dia. Direction Code		Dir ^N	Stress	Duration	Secondary Creep rate	Total Creep Strain %
°C		in.		Tonf/in ²	Hrs.	x 10 ⁻⁶ /Hr.	x 10 ⁻³
100 ±0.4	Z ₁	0.1872	OA	23.60	84.4	3.4	1.823
	X	0.1875	CB	23.55	94.3	3.5	1.41
	Y	0.1880	OC	23.52	115.0	3.5	1.50
150 ±0.4	Z ₂	0.1873	OA	23.58	27.1	120.0	11.36
	ZXY _{ag}	0.1875	AG	23.54	21.0	95.0	7.02
	ZXY _{oe}	0.1883	CE	23.71	21.8	450	27.15
250 ±0.4	Z	.1875	OA	16.3	4.8 min.	-	26.3
	"	.1875	"	15.96	27.0 min.	-	24.0
	"	.1870	"	15.12	32.0 min.	-	18.4
	"	.1875	"	14.23	2.25 Hrs.	-	43.6
	"	.1875	"	13.03	7.0 Hrs.	-	34.0
250 ±0.4	Z ₃	.1878	"	12.10	12.0 Hrs.	-	30.2
	XYZ _{cd}	.1875	CD	12.05	12.5 Hrs.	-	22.6
	XYZ _{fb}	.1880	FB	12.16	9.03 Hrs.	-	29.2

TABLE IX

Incremental Stress Tensile Creep (see Fig. 22)

HIDUMINIUM RR58 DTD 731

At 150°C

Specimens: 1.00 in. gauge length and 0.1875 in. diameter

Specimen Code	Stress Tonf/in ²		Initial elastic plastic strain	Creep Strain	Duration	Sec. Creep Rate
	Nominal	Corrected	$\times 10^{-3}$	$\times 10^{-3}$	Hours	$\times 10^{-6}/\text{Hr}$
<u>XIV</u> E = 4.23 $\times 10^3$ Tonf/in ² ⊙	18.02	18.05	4.45	1.41	231	5.0
	18.91	18.97	5.86	5.46	120	3.9
	19.35	19.52	11.32	7.77	24.0	22.0
	19.82	20.14	19.09	6.73	4.0	-
	20.27	20.73	25.82	7.62	1.2	-
			33.44			
<u>XV</u> E = 4.35 $\times 10^3$ Tonf/in ² ×	18.50	18.53	4.38	0.73	210	3.3
	18.82	18.86	5.11	0.81	102	6.8
	18.98	19.04	5.92	1.60	92	17.7
	19.14	19.23	7.52	7.36	101	46.0
	19.31	19.52	14.88	8.46	34.3	400.0
			23.34			
<u>XXX</u> E = 4.36 $\times 10^3$ Tonf/in ² +	18.12	18.15	4.24	0.24	24	3.10
	18.44	18.48	4.48	0.19	24	4.7
	18.77	18.81	4.67	0.19	24	4.7
	19.41	19.45	4.86	0.37	24	13.0
	19.74	19.79	5.23	0.55	24	19.8
	20.06	20.11	5.78	0.86	24	33.0
	20.39	20.47	6.64	1.66	24	60.0
	20.71	20.82	8.33	4.01	24	148.
	21.04	21.16	12.31	13.4	24	-
			25.71			

TABLE IX (continued)

Incremental Stress Tensile Creep (see Fig. 22)

HIDUMINIUM RR58 DTD 731

At 150°C

Specimens: 1.00 in. gauge length and 0.1875 in. diameter

Specimen Code	Stress Tonf/in ²		Initial elastic plastic strain	Creep Strain	Duration	See Creep Rate
	Nominal	Corrected	× 10 ⁻³	× 10 ⁻³	Hours	× 10 ⁻⁶ /Hr
5 (-) E = 4.36 × 10 ³ Tonf/in ²	20.54	20.57	5.07	0.82	24	17.2
	21.22	21.26	5.89	3.450	24	173.0
	21.83	-	9.35	16.10	8.8	-
			25.45			
<u>XXXII</u> E = 4.34 × 10 ³ Tonf/in ² Δ	19.50	19.53	4.68	0.65	24	13.5
	19.91	19.94	5.33	0.54	24	22.1
	20.31	20.35	5.87	1.20	24	50
	20.72	20.78	7.07	3.67	24	150
	21.13	21.24	10.74	20.40	23	450
			31.13			

TABLE X

Incremental Stress Tensile Creep (see Fig. 23)

HIDUMINIUM RR58 DTD 731

At 200°C

Specimens: 1.00 in. gauge length and 0.1875 in. diameter.

Specimen Code	Stress σ_x		Initial Elastic-Plastic $\times 10^{-3}$	Creep Strain $\times 10^{-3}$	Duration Hours	Secondary Creep Rate $\times 10^{-6}/\text{Hr}$
	Nom.	Corr.				
XIII + E = 4.21 $\times 10^3$ Tonf/in ²	12.87	12.88	3.05	1.9	95	17.0
	13.19	13.23	4.05	4.52	42	96.0
	13.35	13.45	9.47	6.47	23	250.0
	13.52	13.70	15.94	5.28	9.0	560.0
	13.69	13.95	21.22	32.90	15.2	-
			54.12			
XVI • E = 4.20 $\times 10^3$ Tonf/in ²	12.82	12.83	3.06	2.63	119	15.0
	12.98	13.01	5.69	22.75	106	-
			28.44			
XI ○ E = 4.23 $\times 10^3$ Tonf/in ²	12.22	12.23	2.90	0.23	21	5.0
	12.39	12.41	3.13	0.14	21	3.0
	12.54	12.56	3.27	0.10	24	2.0
	12.70	12.72	3.37	0.13	24	2.5
	12.87	12.89	3.50	0.12	26	2.8
	13.02	13.04	3.62	0.10	24	2.8
	13.19	13.21	3.72	0.11	22	2.9
	13.90	13.93	3.83	0.46	24	9.4
	14.48	14.51	4.29	0.71	24	18.7
	14.78	14.82	5.00	1.45	24	55.0
	15.11	15.18	6.45	5.49	24	218.0
	15.44	15.59	12.04	6.60	7.6	740. ?
			18.64			

TABLE X (continued)

Specimen Code	Stress		Initial Elastic-Plastic	Creep Strain	Duration	Secondary Creep Rate
	Tonf/in ²		× 10 ⁻³	× 10 ⁻³	Hours	× 10 ⁻⁶ /Hr
	Nom.	Corr.				
XVII E = 4.28 × 10 ³ Tonf/in ² ×	11.56	11.57	2.67	0.58	96	1.1
	11.72	11.74	3.25	0.50	96	5.0
	11.88	11.91	3.75	0.91	93	8.8
	12.04	12.08	4.66	2.24	95	22.6
	12.21	12.25	6.90	6.00	96	58.0
	12.37	12.50	12.90	3.56	24	144.0
	12.53	12.71	16.46	6.90	24	250.0
			23.36			
XXV H E = 4.15 × 10 ³ Tonf/in ²	11.00	11.01	2.45	0.42	24	10.0
	11.32	11.33	2.87	0.23	24	6.0
	11.65	11.66	3.10	0.20	24	6.0
	11.97	12.00	3.30	0.31	24	9.8
	12.30	12.32	3.61	0.42	24	15.0
	12.62	12.65	4.03	1.17	24	32.0
	12.95	12.99	5.20	3.35	23	135.0
			8.55			
XXIX Δ E = 4.19 × 10 ⁻³ Tonf/in ²	12.30	12.31	2.94	0.55	24	19.0
	12.62	12.64	3.49	0.69	24	26.0
	12.94	12.97	4.18	2.22	24	88.0
	13.27	13.32	6.40	8.31	24	340.0
	13.60	13.77	14.71	13.04	12.6	-
			27.75			

TABLE X (continued)

Specimen Code	Stress		Initial Elastic-Plastic	Creep Strain	Duration	Secondary Creep Rate
	Tonf/in ² Nom. Corr.		× 10 ⁻³	× 10 ⁻³	Hours	× 10 ⁻⁶ /Hr
XXXII □ E = 4.11 × 10 ³ Tonf/in ²	12.30	12.31	3.00	0.96	24	20.0
	12.58	12.61	3.96	0.57	24	18.0
	12.85	12.88	4.53	1.77	24	73.0
	13.13	13.18	6.30	6.40	24	260.0
	13.40	13.53	12.70	33.60	23.5	840.0
			46.3			
3 (-) E = 4.19 × 10 ³ Tonf/in ²	12.62	12.63	3.08	0.31	25	7.0
	13.02	13.04	3.39	0.22	24	8.0
	13.43	13.45	3.61	0.32	24	12.0
	13.83	13.86	3.93	0.61	24	24.0
	14.24	14.28	4.54	2.80	24	116.0
	14.56	14.63	7.34	12.20	21.3	510.0
			19.54			

TABLE XI

Incremental Stress Tensile Creep (see Fig. 24)

HIDUMINIUM RR58 DTD 731

At 250°C

Specimens: 1.00 in. gauge length and 0.1875 in. diameter

Specimen Code	Stress		Initial Elastic-plastic Strain	Creep Strain	Duration	Secondary Creep Rate
	Tonf/in ²		10 ⁻³ %	10 ⁻³ %	Hrs	10 ⁻⁶ /Hr
	Nom.	Corr.				
X E = 3.47 × 10 ³ Tonf/in ²	11.01	11.02	2.75	1.58	42.0	29.0
	11.26	11.29	4.33	8.16	48.0	73.0
	11.42	11.53	12.49	36.54	22.0	610.0
			49.03			
XX E = 4.01 × 10 ³ Tonf/in ²	8.09	8.10	2.45	1.75	24.0	27.0
	8.27	8.29	4.20	0.73	24.0	27.0
	8.57	8.59	4.93	1.26	24.0	48.0
	8.82	8.86	6.49	12.25	50.0	115.0
			19.44			
XXI E = 3.97 × 10 ³ Tonf/in ²	7.93	7.94	2.11	1.16	24.0	30.0
	8.13	8.15	3.27	1.03	24.0	42.0
	8.33	8.36	4.30	2.27	24.0	91.0
	8.54	8.58	6.57	9.70	24.0	320.0
	8.74	8.85	16.27	11.03	5.7	-
			27.30			
XXVI E = 3.88 × 10 ³ Tonf/in ²	7.93	7.94	2.07	1.06	24.0	26.0
	8.17	8.19	3.13	0.77	24.0	30.0
	8.28	8.30	3.90	1.00	18.9	49.0
	8.44	8.47	4.90	0.53	5.1	-
	8.60	8.63	5.43	7.62	24.0	210.0
	8.95	9.05	13.05	15.05	10.0	-
			28.10			

TABLE XI (continued)

Specimen Code	Stress		Initial Elastic-plastic Strain	Creep Strain	Duration	Secondary Creep Rate
	Tonf/in ²		10 ⁻³ %	10 ⁻³ %	Hrs	10 ⁻⁶ /Hr
	Nom.	Corr.				
XXVIII Δ E = 3.88 × 10 ³ Tonf/in ²	7.77	7.78	2.01	0.47	24.0	11.0
	7.98	7.99	2.48	0.30	24.0	12.0
	8.20	8.21	2.78	0.28	19.7	13.5
	8.26	8.27	3.06	0.18	4.3	-
	8.49	8.51	3.24	0.89	24.0	36.0
	8.72	8.74	4.13	2.51	24.0	107.0
	8.88	8.92	6.64	7.58	22.3	210.0
			14.22			
2 □ E = 4.06 × 10 ³ Tonf/in ²	8.65	8.66	2.13	26.16	140	36.0
			28.29			
4 (·) E = 4.02 × 10 ³ Tonf/in ²	8.75	8.77	2.22	0.56	24.0	16.0
	9.03	9.04	2.78	0.37	24.0	15.0
	9.30	9.32	3.15	1.41	24.0	62.0
	9.55	9.58	4.56	10.30	24.0	200.0
	9.82	9.94	14.86	32.00	6.0	-
			46.86			

TABLE XII

Incremental Stress Tensile Creep (see Fig. 25)

HIDUMINIUM RR58 DTD 731

At 150°C

Derived parameters

Specimens: 1.00 in. gauge length and 0.1875 in. diameter.

ϵ_p = Instantaneous plastic and creep strain.

Specimen Code	Stress	ϵ_p	$U_{sh} + W_p$	$\dot{\epsilon}_x$	$\sigma_{ei} \dot{\epsilon}_{ei}$
	Tonf/in ²	$\times 10^{-3}$	$\frac{i}{\times 10^{-3}} \text{ in Tonf/in}^3$	$\times 10^{-6}/\text{Hr}$	$\frac{i}{\text{Hr} \times 10^{-6}} \text{ in Tonf/in}^3$
XIV ○ E = 4.34 x 10 ³ Tonf/in ²	18.05	0.19	3.76	5.0	90.3
	18.97	1.60	6.64	3.9	74
	19.52	7.06	17.56	22.0	429
	20.14	14.83	-	-	-
	20.73	21.56	-	-	-
XV x	18.53	0.09	3.61	3.3	61.0
	18.86	0.82	5.10	6.8	128
	19.04	1.63	6.73	17.7	337
	19.23	3.23	9.92	46.0	884
	19.52	10.59	24.48	400.0	7800
XXX +	18.15	0.06	3.41	3.1	56
	18.48	0.30	3.97	4.7	87
	18.81	0.50	4.48	4.7	89
	19.45	0.70	5.14	13.0	253
	19.79	1.07	6.04	19.8	392
	20.11	1.62	7.30	33.0	664
	20.47	2.48	9.27	60.0	1230
	20.82	4.14	12.95	148.0	3080

TABLE XII (continued)

Specimen Code	Stress	p	$U_{sh} + W_p$		
	Tonf/in ²	$\times 10^{-3}$	in Tonf/in ³ $\times 10^{-3}$	$\times 10^{-6}$ /Hr	in Tonf/in ³ / Hr $\times 10^{-6}$
5 (-)	20.57	0.43	5.15	17.2	353
	21.22	1.25	7.15	173	3670
	21.83	4.71	15.06	-	
XXXII Δ	19.53	0.18	4.17	13.5	264
	19.44	0.83	5.63	22	440
	20.35	1.37	7.93	50	1020
	20.78	2.57	9.75	150	3120
	21.24	6.24	17.75	450	9550

TABLE XIII

Incremental Stress Tensile Creep (see Fig. 26)

HIDUMINIUM RR58 DTD 731

At 200° C

Derived parameters

Specimens: 1.00 in. gauge length and 0.1875 in. diameter

Specimen Code	Stress	Strain Plastic	$U_{sh} + W_p$	$\dot{\epsilon}_x$	$\bar{\sigma}_{ei} \dot{\epsilon}_{ei}$
	Tonf/in ²	$\times 10^{-3}$	in tonf/in ³	$\times 10^6/\text{Hr}$	in tonf/in ³ /Hr
XIII + $E = 4.19 \times 10^3$ Tonf/in ²	12.88	0	1.71	17	219
	13.23	1.9	4.33	96	1280
	13.45	6.42	10.50	250	3360
	13.70	12.89	19.59	560	7760
	13.95	18.17	-	-	-
XVI .	12.83	0	1.70	15	192
	13.01	-	-	-	-
XI ○	12.23	0	1.54	5.0	61
	12.41	0.23	1.88	3.0	37
	12.56	0.37	2.09	2.0	25
	12.72	0.47	2.27	2.5	32
	12.89	0.60	2.49	2.8	36
	13.04	0.72	2.70	2.8	37
	13.21	0.82	2.88	2.9	38
	13.93	0.93	3.30	9.4	131
	14.51	1.39	4.19	18.7	271
	14.82	2.10	5.37	55	815
	15.18	3.55	7.78	218	3310

TABLE XIII (continued)

Specimen Code	Stress	Strain Plastic	$U_{sh} + W_p$	$\dot{\epsilon}_x$	$\sigma_{ei} \dot{\epsilon}_{ei}$
	Tonf/in ²	$\times 10^{-3}$	in Tonf/in ³	$\times 10^6$ /Hr	in tonf/in ³ /Hr
XXVII x	11.57	0	1.38	1.1	13
	11.74	0.58	2.10	5.0	59
	11.91	1.08	2.76	8.8	105
	12.08	1.99	3.92	22.6	273
	12.25	4.23	6.73	58	710
	12.50	10.23	14.37	144	1800
	12.71	13.79	19.17	250	3180
XXV H	11.01	0	1.25	10	110
	11.33	0.42	1.79	6	68
	11.67	0.65	2.16	6	70
	12.00	0.85	2.51	9.8	118
	12.32	1.16	2.99	15	185
	12.65	1.58	3.65	38	481
	12.99	2.75	5.32	135	1755
XXIX Δ	12.31	0	1.56	19	224
	12.64	0.55	2.34	26	329
	12.97	1.24	3.34	88	1140
	13.32	3.46	6.43	340	4530
	13.77	11.77	18.16	-	-
XXXII □	12.31	0	1.56	20	246
	12.61	0.96	2.83	18	227
	12.88	1.53	3.68	73	930
	13.18	3.30	6.14	260	3430
	13.53	9.70	15.02	840	11350
3 (c)	12.63	0.07	1.73	7.0	88
	13.04	0.38	2.26	8.0	104
	13.45	0.60	2.68	12.0	161
	13.86	0.92	3.26	24.0	333
	14.28	1.53	4.27	116.0	1660
	14.63	4.33	8.52	510.0	7460

TABLE XIV

Incremental Stress Tensile Creep (see Fig. 27)

ALUMINIUM RR58 DTD 731

At 250°C

Derived parameters

Specimens: 1.00 in. gauge length and 0.1875 in. diameter

Specimen Code	Stress	Strain plastic	$U_{sh} + W_p$	$\dot{\epsilon}_x$	$\delta\epsilon_i \dot{\epsilon}_{ei}$
	Tonf/in ²	$\times 10^{-3}$	in Tonf/in ³	$\times 10^{-6}/Hr$	in Tonf/in ³ / Hr
X • E = 3.97 x 10 ² Tonf/in ²	11.02	0	1.33	29	319
	11.29	1.58	3.18	73	825
	11.53	9.74	12.69	610	7030
XX ⊖	8.10	0.45	1.08	27	219
	8.29	2.20	2.59	27	224
	8.59	2.93	3.33	48	413
	8.89	4.19	4.75	115	1020
XXI ×	7.94	0.10	0.77	30	238
	8.15	1.26	1.76	42	342
	8.36	2.29	2.69	91	760
	8.58	4.56	4.73	320	2740
	8.85	14.26	13.45	-	-
XXVI +	7.94	0.08	0.75	26	217
	8.19	1.14	1.67	30	245
	8.30	1.91	2.35	49	407
	8.47	2.91	-	-	-
	8.63	3.44	3.77	210	1810
	9.05	10.06	-	-	-

TABLE XIV (continued)

Specimen Code	Stress	Strain Plastic	$U_{sh} + W_p$	$\dot{\epsilon}_x$	$\sigma_{ei} \dot{\epsilon}_{ei}$
	Tonf/in ²	$\times 10^{-3}$	in Tonf/in ³	$\times 10^{-6}$ /Hr	in Tonf/in ³ / Hr
XXVIII	7.78	0.05	0.71	11	76
	7.99	0.52	1.12	12	96
	8.21	0.82	1.141	13.5	111
	8.27	1.10	-	-	-
	8.51	1.28	1.89	36	306
	8.74	2.17	2.24	107	936
	8.92	4.68	5.05	210	1880
2	8.65	0.10	0.92	36	312
4	8.77	0.10	0.94	16	141
	9.04	0.66	1.50	15	136
	9.32	1.03	1.92	62	578
	9.58	2.44	3.34	200	1920
	9.94	12.74	-	-	-

TABLE XV

Main Test Programme (Tubular Specimens) (see Figs. 28 to 39)

HIDUMINIUM RR58 DTD 731

Gauge Length 4.00 in. Outer Dia. 1.100 in. Inner Dia. 1.000 in.

Test Temperature 150°C

$$\epsilon_e = \left[\frac{\sigma_x^2}{3T/T_0} + \epsilon_{xy}^2 \right]^{1/2}$$

$$\dot{\gamma}_e = \left[3 \frac{T}{T_0} \dot{\epsilon}_x^2 + \dot{\gamma}_{xy}^2 \right]^{1/2}$$

Code	Stress Tonf/in ²		Strain Initial		Creep Strain		Duration Hrs	Creep Rate	
	ϵ_{xy}	σ_x	Elastic x 10 ⁻³	plastic	x 10 ⁻³			10 ⁻³ /Hour	
	Shear	Tensile	Shear	Direct	Shear	Direct		$\dot{\gamma}_{xy}$	$\dot{\epsilon}_x$
T.42 -/-	-	18.51	-	4.38	-	0.35	24	-	5.1
	-	19.10	-	4.73	-	0.265	24	-	10.2
	-	19.62	-	4.995	-	0.535	24	-	13.4
	-	20.15	-	5.530	-	1.125	24	-	36.5
	-	20.67	-	6.655	-	2.55	22.4	-	68.0
T.23 •	10.17	-	6.12	-	0.806	-	24	8.0	-
	10.42	-	6.926	-	0.354	-	24	14.0	-
	10.76	-	7.280	-	0.700	-	24	26.4	-
	11.06	-	7.980	-	1.170	-	24	46.0	-
	11.35	-	9.150	-	2.500	-	24	89.0	-
	11.65	-	11.650	-	6.240	-	10.2	198	-
T.37 ⊕	9.94	4.97	5.95	2.98	0.51	0.02	24	10.8	?
	10.22	5.11	6.46	3.00	0.48	0.40	24	17.8	3.1
	10.51	5.25	6.94	3.40	0.80	0.61	24	29.6	5.0
	10.79	5.39	7.74	4.01	1.66	1.31	24	60.2	8.7
	11.07	5.54	9.40	5.32	2.36	2.16	24	98.3	16.1

TABLE XV (continued)

Specimen Code	Stress Tonf/in ²		Strain Initial Elastic $\times 10^{-3}$ plastic		Creep Strain $\times 10^{-3}$		Duration Hrs	Creep Rate 10^{-6} /hour	
	τ_{xy} Shear	σ_x Tensile	Shear	Direct	Shear	Direct		$\dot{\gamma}_{xy}$ Shear	$\dot{\epsilon}_x$ Direct
T.38 X	6.88	13.77	4.11	3.26	0.28	0.175	24	6.5	4.5
	7.12	14.28	4.39	3.435	0.295	0.223	24	12.0	8.0
	7.32	14.65	4.685	3.658	0.55	0.328	24	22.9	12.0
	7.53	15.05	5.235	3.986	0.95	0.620	24	40.4	26.0
	7.72	15.40	6.185	4.606	2.05	1.00	24	80.5	41.0
T.39 Δ	9.00	9.00	5.45	2.11	0.52	0.23	24	8.9	5.4
	9.38	9.00	5.97	2.34	0.304	0.124	24	18.6	5.0
	9.71	9.00	6.274	2.464	0.79	0.22	24	31.1	9.2
	10.04	9.00	7.064	2.684	1.52	0.39	24	59	16.2
	10.38	9.00	8.584	3.07	2.38	0.67	24	97	28.0
T.40 $\square$	9.00	9.00	5.40	2.15	0.53	0.30	24	9.1	6.9
	9.00	10.21	5.93	2.45	0.45	0.166	24	18.0	5.7
	9.00	11.18	6.38	2.616	0.71	0.245	24	29.4	10.7
	9.00	12.10	7.09	2.861	1.21	0.460	24	48.0	19.8
	9.00	12.97	8.30	3.321	2.39	0.820	24	83	33.7

TABLE XVI

Main Test Programme (Tubular Specimens) (see Fig. 38 to 48)

HIDUMINIUM RR58 DTD 731

Gauge Length 4.00 in.

Outer Dia. 1.00 in.

Inner Dia. 1.000 in.

Test Temperature 250°C

$$\tau_e = \left[\frac{\sigma_x^2}{3T/T_0} + \tau_{xy}^2 \right]^{1/2}$$

$$\dot{\gamma}_e = \left[3 \frac{T}{T_0} \dot{\epsilon}_x^2 + \dot{\gamma}_{xy}^2 \right]^{1/2}$$

Specimen Code	Stress Tonf/in ²		Strain Initial Elastic x 10 ⁻³ plastic		Stage Creep Strain x 10 ⁻³		Duration Hrs	Secondary Creep Rate 10 ⁻⁶ /Hr	
	Shear τ_{xy}	Tensile σ_x	Shear	Direct	Shear	Direct		Shear $\dot{\gamma}_{xy}$	Direct $\dot{\epsilon}_x$
T.28	-	8.51	-	2.18	-	0.202	24	-	6.9
	-	8.77	-	2.382	-	0.309	24	-	12.6
	-	9.04	-	2.691	-	0.451	24	-	19.0
	-	9.30	-	3.142	-	0.854	24	-	36.4
	-	9.57	-	3.996	-	1.710	24	-	71.0
	-	9.83	-	5.706	-	7.87	22.8	-	91.0
T.20	4.52	-	2.825	-	1.725	-	24	54	-
	4.65	-	4.550	-	1.963	-	24	84	-
	4.77	-	6.513	-	3.880	-	24	166	-
	4.91	-	10.393	-	5.720	-	9.6	400	-
T.21	4.38	-	2.815	-	1.17	-	24	30.8	-
	4.52	-	3.985	-	1.185	-	24	48	-
	4.65	-	5.170	-	1.935	-	24	79	-
	4.77	-	7.075	-	4.670	-	24	182	-
	4.91	-	11.745	-	2.760	-	4.6	367	-
T.30 ⊙	4.69	2.21	3.11	0.60	4.30	0.43	24	196	17.7
	4.38	2.19	7.41	1.03	1.03	0.202	24	41.6	8.0
	4.51	2.25	8.44	1.232	1.91	0.272	24	82.5	10.9
	4.65	2.32	10.35	1.504	3.99	0.450	24	168	18.3
	4.76	2.38	14.34	1.954	6.12	0.480	6.8	355	42.2

TABLE XVI (continued)

Specimen Code	Stress Tonf/in ²		Strain Initial Elastic $\times 10^{-3}$ plastic		Stage Creep Strain $\times 10^{-3}$		Duration Hrs	Secondary Creep Rate $10^{-6}/\text{Hr}$	
	τ_{xy}	σ_x	Shear	Direct	Shear	Direct		$\dot{\gamma}_{xy}$	$\dot{\epsilon}_x$
T.31 x	3.10	6.20	1.98	1.56	0.71	0.158	24	20	5.4
	3.19	6.38	2.69	1.718	0.80	0.455	24	32.9	16.4
	3.29	6.57	3.57	2.173	1.58	0.670	24	63	28.6
	3.38	6.76	5.15	2.843	2.80	1.200	24	116	56.6
	3.47	6.95	7.95	3.143	6.18	3.480	17.8	261	128
T.33 Δ	3.92	3.92	2.51	1.02	1.08	0.404	24	22	7.1
	3.92	4.48	3.59	1.424	0.71	0.298	24	29.4	12.0
	3.92	4.94	4.30	1.722	1.56	0.580	24	63.8	23.4
	3.92	5.46	5.86	2.302	2.38	1.170	24	115.4	47.0
	3.92	5.92	8.24	3.472	6.22	3.780	24.1	252	98.0
T.34 E	3.92	3.92	2.53	1.00	0.85	0.148	24	23.8	4.2
	4.07	3.92	3.38	1.148	1.18	0.225	24	42.6	9.0
	4.21	3.92	4.56	1.373	1.65	0.302	24	73.0	13.0
	4.36	3.92	6.21	1.675	3.55	0.638	24	147.0	27.0
	4.51	3.92	9.76	2.313	6.22	1.730	19.1	310.0	62.4

$$\tau_e = \left[\frac{\sigma_x^2}{3T/T_0} + \tau_{xy}^2 \right]^{1/2}, \quad \gamma_e = \left[3 \frac{T}{T_0} \dot{\epsilon}_x^2 + \dot{\gamma}_{xy}^2 \right]^{1/2}$$

Specimen Code	τ_e	$\dot{\gamma}_e$	Strain Creep + Plastic		Secondary Work Rate		γ_e	$U_{sh} + W_p$	$\tau_e \dot{\gamma}_e$
	Tonf/in ²	10 ⁻⁶ /Hr	γ_p	ϵ_p	$\tau_{xy} \dot{\gamma}_{xy}$	$\sigma_x \dot{\epsilon}_x$	10 ⁻³	inTonf/in ³	inTonf/in ³
T.42 -.- -.-	10.17	9.3	-	0.40	-	94.5	0.73	35.0	94.5
	10.49	18.6	-	0.665	-	195	1.22	41.4	195
	10.77	24.4	-	1.200	-	263	2.19	46.1	263
	11.03	66.4	-	2.325	-	736	4.29	56.2	736
	11.36	124.0	-	4.875	-	1410	8.92	78.6	1410
							131.0		
T.23 •	10.17	8.0	0.85	-	81.4	-	0.85	31.3	81.4
	10.42	14.0	1.20	-	146	-	1.20	39.5	146
	10.76	26.4	1.90	-	284	-	1.90	43.2	284
	11.06	46.0	3.07	-	519	-	3.07	50.7	519
	11.35	89.0	5.57	-	1010	-	5.57	63.7	1010
	11.65	198	11.99	-	2310	-	11.99	92.0	2310
							167.0		
T.37 ⊙	10.30	-	0.2	0.02	-	-	0.203	34.0	-
	10.30	15	0.51	0.02	-	-	0.512	36.1	155.
	10.60	18.7	0.48	0.078	-	-	0.501	41.4	198
	10.85	31.0	0.80	0.116	-	-	0.828	46.7	337
	11.19	62.3	1.66	0.210	-	-	1.703	55.7	697
	1.48	103.0	2.36	0.3	-	-	2.465	74.80	1180
							103.0		

TABLE XVII (continued)

[illegible]

TABLE XIX

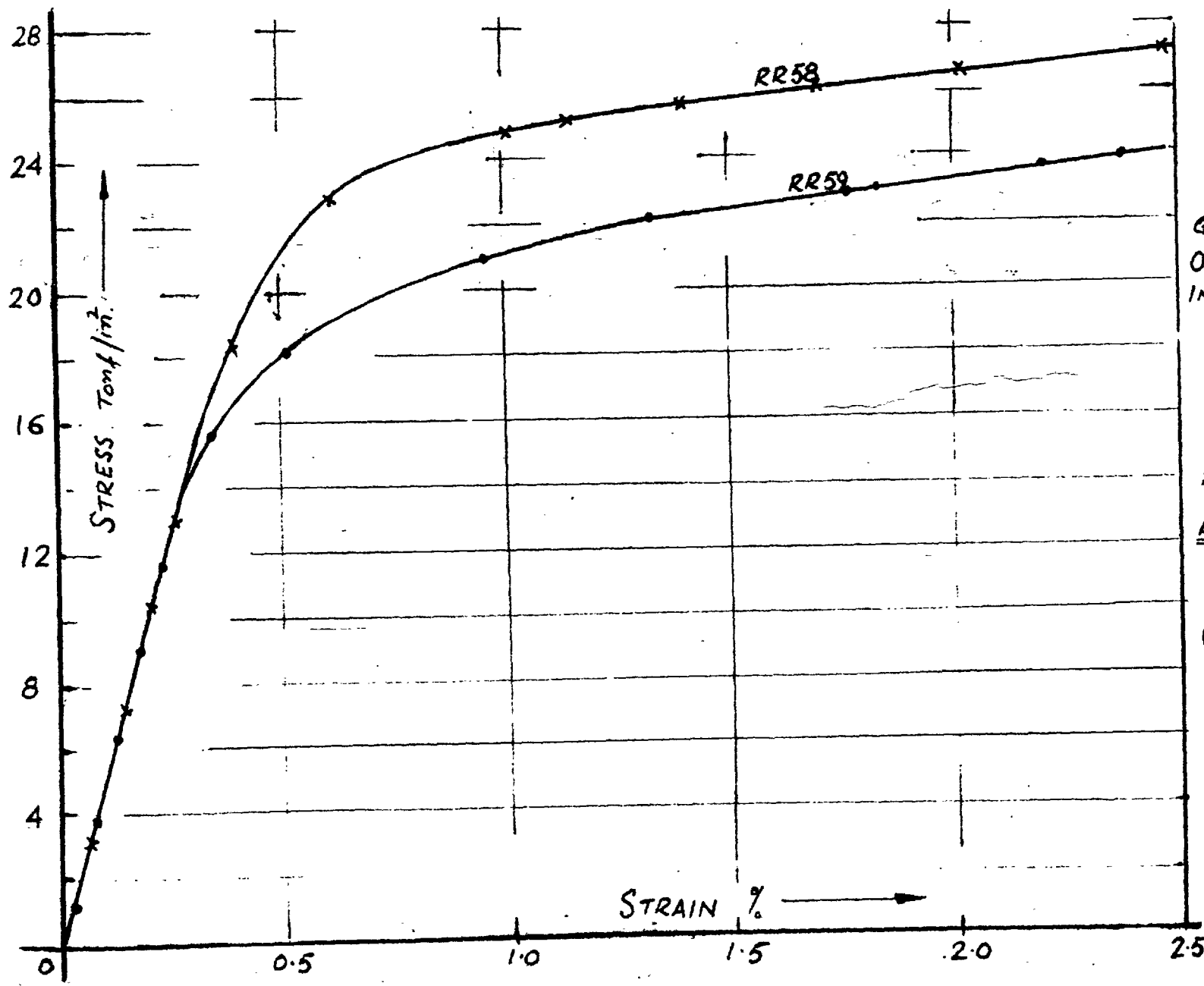
Main Test Programme (see Figs. 49 and 50)

HIDUMINIUM RR58 Spec. DTD 731

Analytical data of incremental stress combined load creep tests.

σ_x = direct stress Component, $\dot{\epsilon}_x$ = Secondary direct creep rate
 τ_{xy} = Shear stress component, $\dot{\gamma}_{xy}$ = " shear creep rate

Temp	Spec Code	1st Load		2nd Load		3rd Load		4th Load		5th Load	
		$\dot{\gamma}_{xy}/\tau_{xy}$	$\dot{\epsilon}_x/\sigma_x$	$\dot{\gamma}_{xy}/\tau_{xy}$	$\dot{\epsilon}_x/\sigma_x$	$\dot{\gamma}_{xy}/\tau_{xy}$	$\dot{\epsilon}_x/\sigma_x$	$\dot{\gamma}_{xy}/\tau_{xy}$	$\dot{\epsilon}_x/\sigma_x$	$\dot{\gamma}_{xy}/\tau_{xy}$	$\dot{\epsilon}_x/\sigma_x$
150° C	T.37	1.09	-	1.74	0.61	2.82	0.95	5.58	1.61	8.88	2.90
	T.38	0.94	0.326	1.69	0.56	3.13	0.82	5.36	1.73	10.42	2.66
	T.39	0.99	0.60	1.98	0.555	3.20	1.02	5.87	1.80	9.34	3.11
	T.40	1.12	0.766	2.00	0.558	3.25	0.958	5.33	1.635	9.22	2.60
250° C	T.30	41.8	8.00	9.50	3.65	18.30	4.85	36.10	7.89	74.7	17.7
	T.31	6.46	0.87	10.32	2.57	19.15	4.35	34.35	8.39	75.20	18.42
	T.33	5.61	1.81	7.50	2.68	16.25	4.74	29.45	8.62	64.3	16.54
	T.34	6.07	1.07	10.50	2.29	17.35	3.32	33.70	6.89	68.7	15.9



TUBULAR SPECIMENS.

GAUGE LENGTH 4.00 in

OUTER DIA 1.10 in

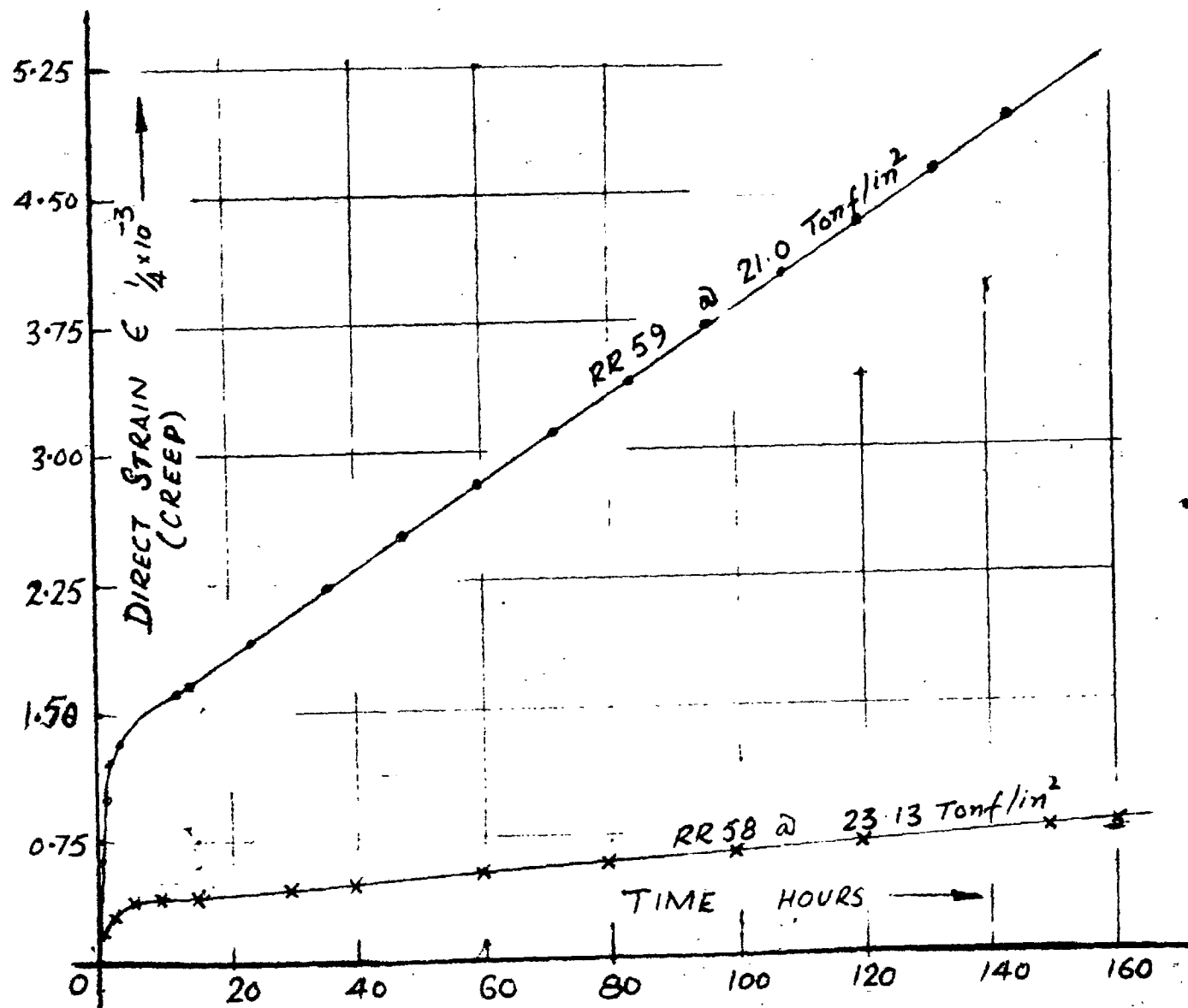
INNER DIA 1.00 in

SPECIMEN CODE	MATL
'A' •	RR59
'B' x	RR58

STATIC TENSILE TESTS
AT ROOM TEMPERATURE

FIG. 9

(material selection)



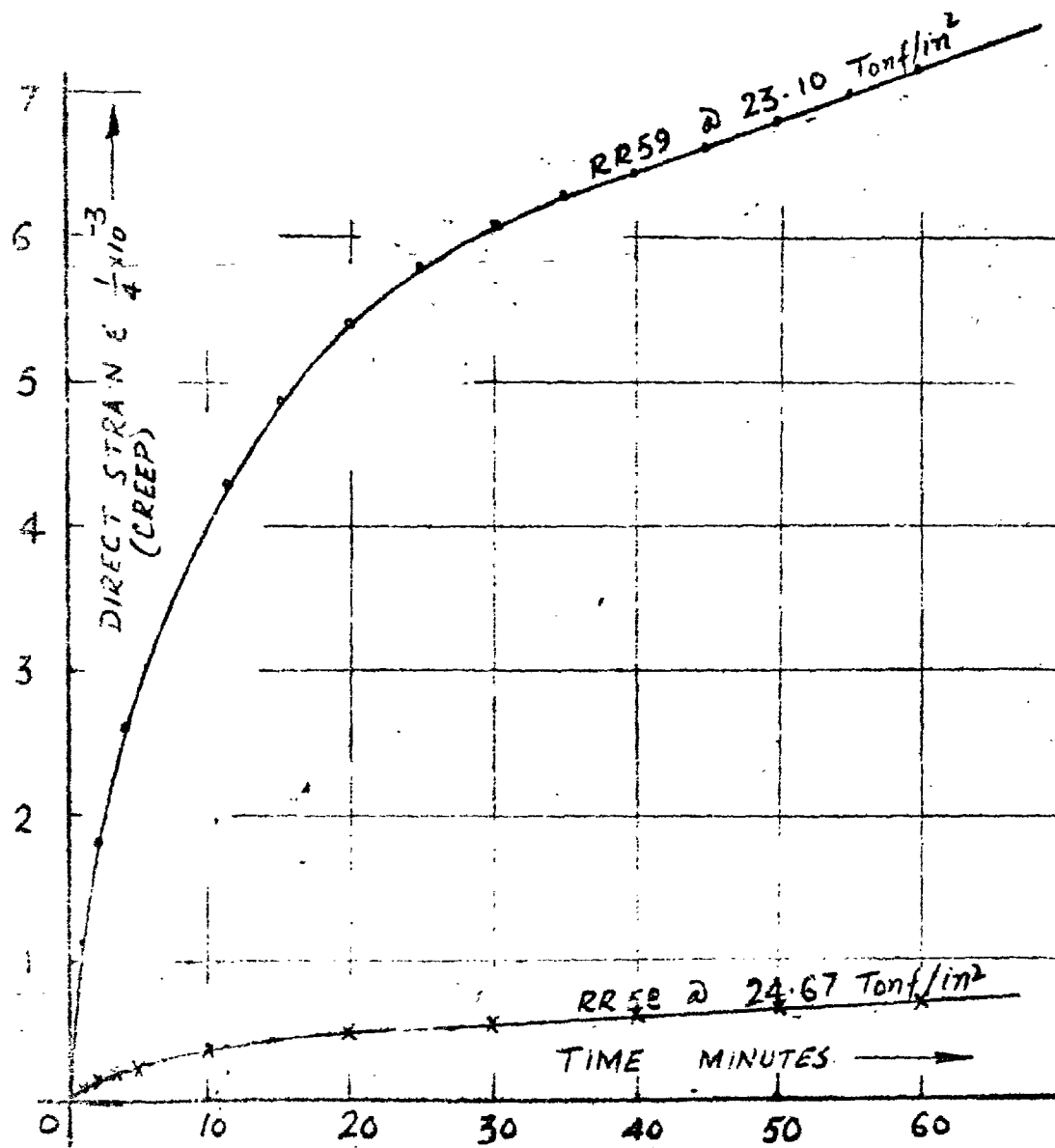
TUBULAR SPECIMENS.

SPECIMEN CODE		MATL
'C'	.	RR59
'D'	x	RR58

CONSTANT STRESS TENSILE CREEP

AT ROOM TEMPERATURE
FIRST LOAD STAGE.

FIG. 10
(material selection)



TUBULAR
SPECIMENS.

SPECIMEN CODE		MAT'L
'C'	*	RR59
'D'	*	RR58

CONSTANT STRESS TENSILE
CREEP

AT ROOM TEMPERATURE

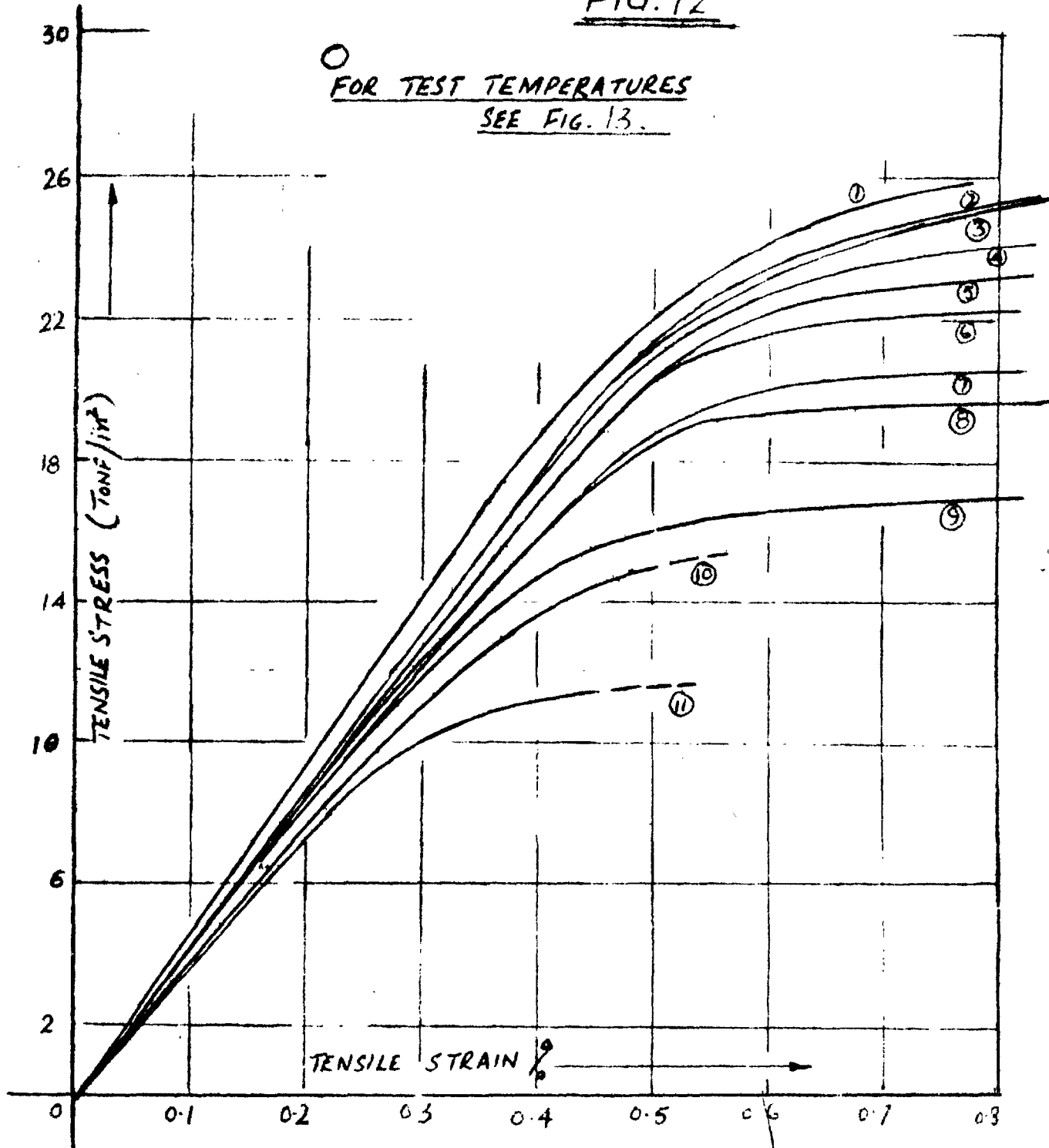
SECOND LOAD STAGE.

FIG. 11
(material selection)

STATIC TENSILE TESTS AT ELEVATED TEMPERATURES
FOR HYDUMINIUM-RR5B (DTD 731)
ONE INCH GAUGE LENGTH & 0.1875 INCH DIAMETER

FIG. 12

○
FOR TEST TEMPERATURES
SEE FIG. 13.

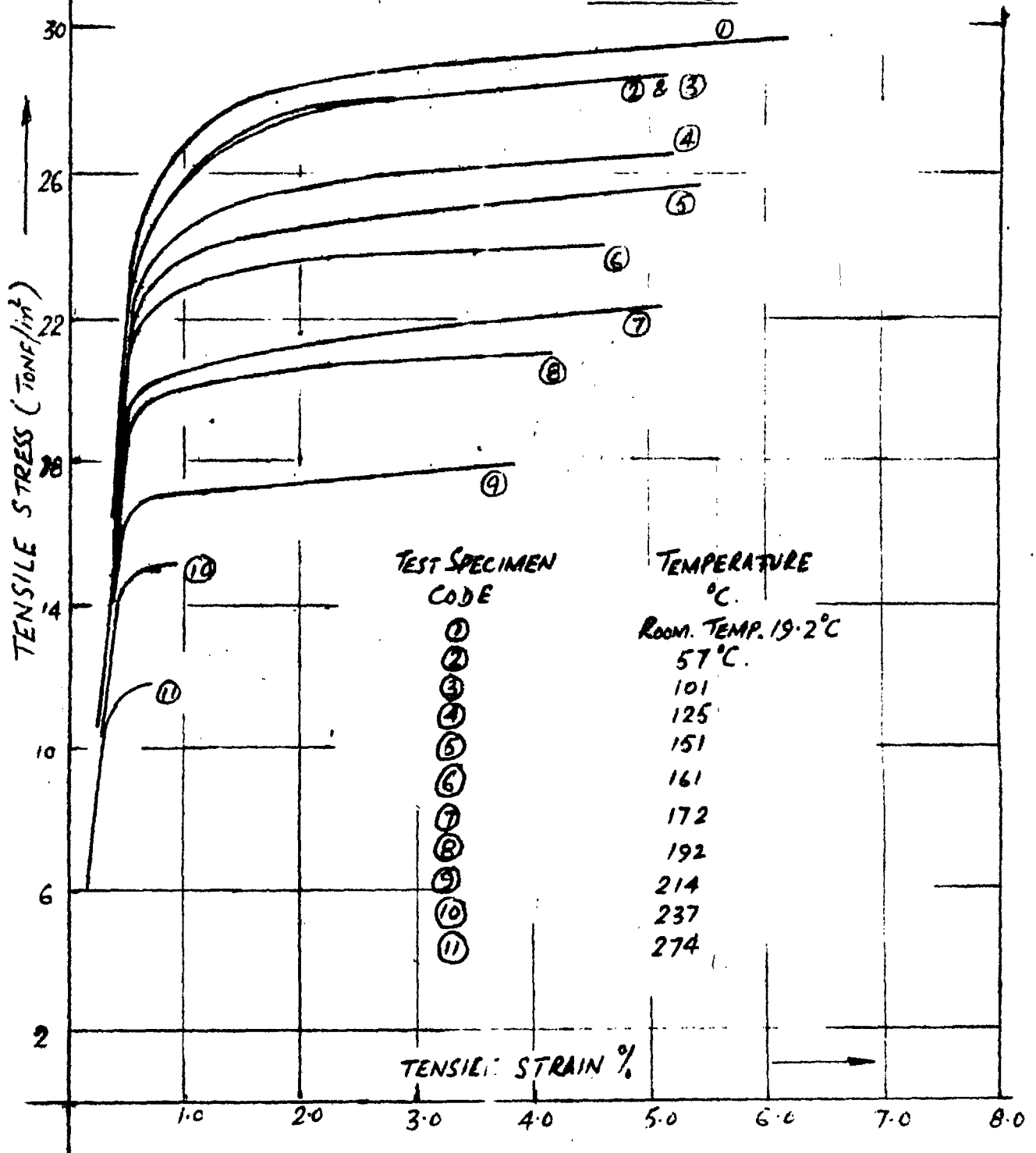


STATIC TENSILE TESTS AT ELEVATED TEMPERATURES

FOR ALUMINIUM-RR58 (DTD 731)

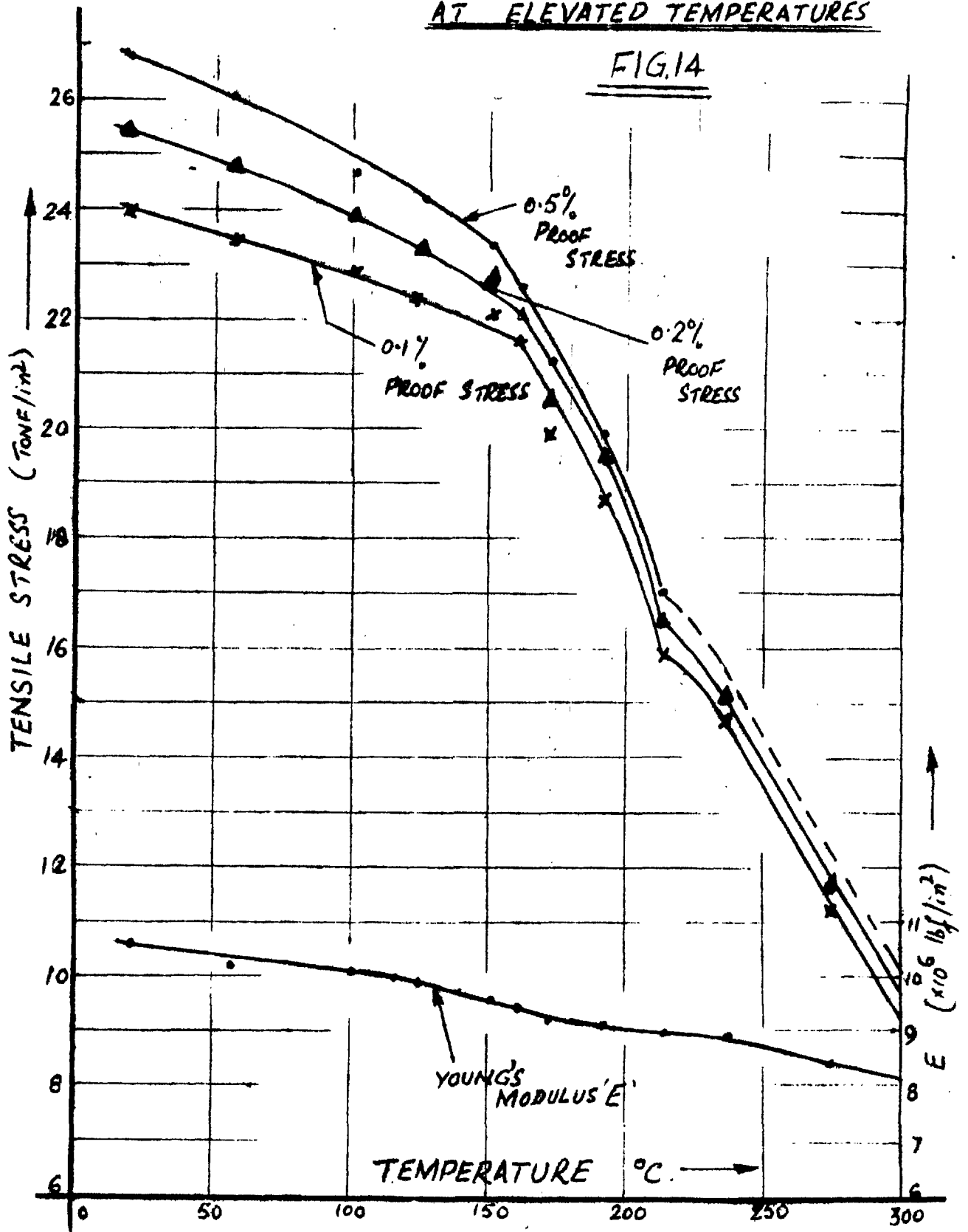
ONE INCH GAUGE LENGTH & 0.1875 INCH DIAMETER.

FIG. 13



VARIATION OF TENSILE PROPERTIES
of ALUMINIUM-RR58 (DTD731)
AT ELEVATED TEMPERATURES

FIG.14

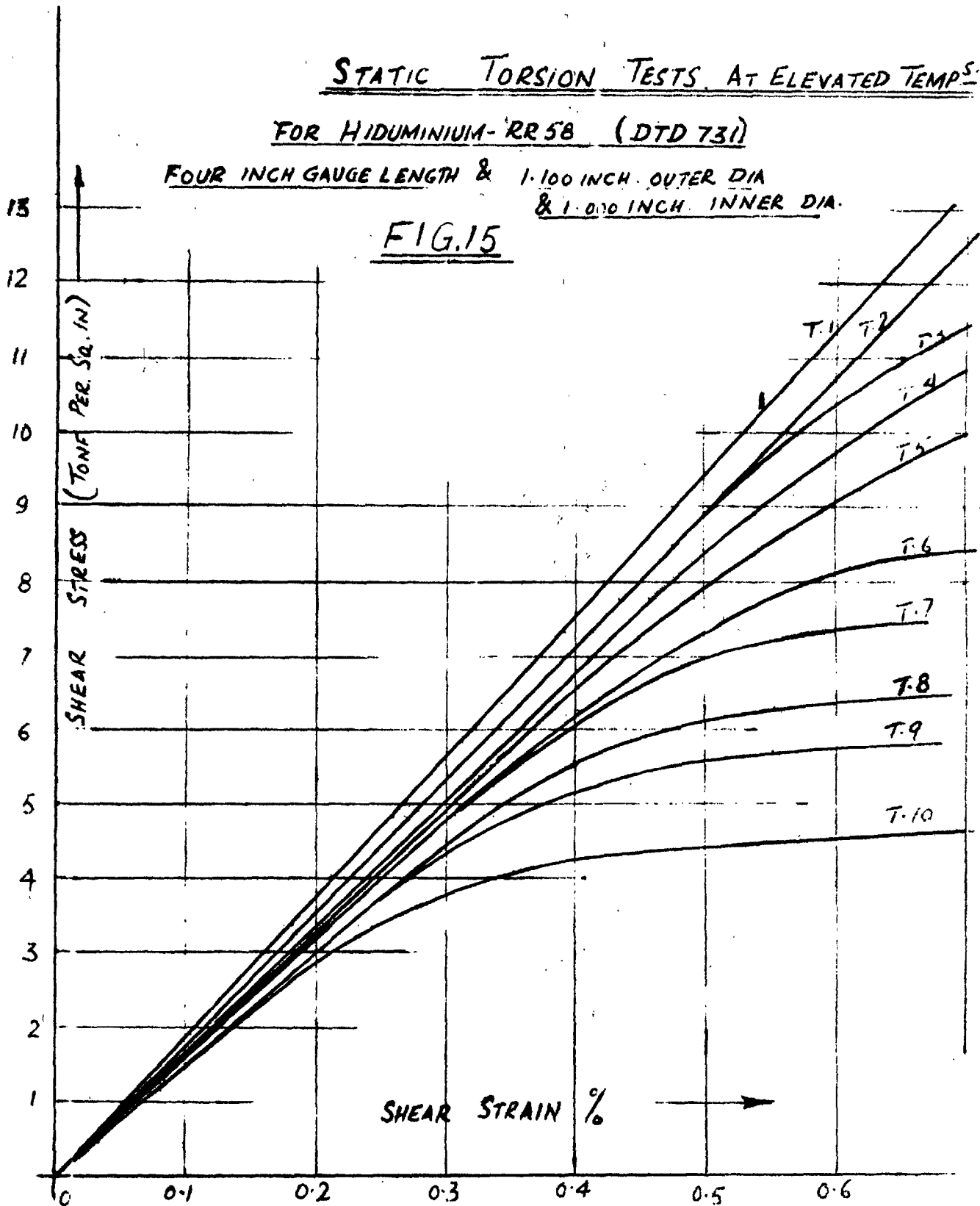


STATIC TORSION TESTS AT ELEVATED TEMPS.

FOR HYDUMINIUM-RR58 (DTD 731)

FOUR INCH GAUGE LENGTH & 1.100 INCH. OUTER DIA
& 1.000 INCH. INNER DIA.

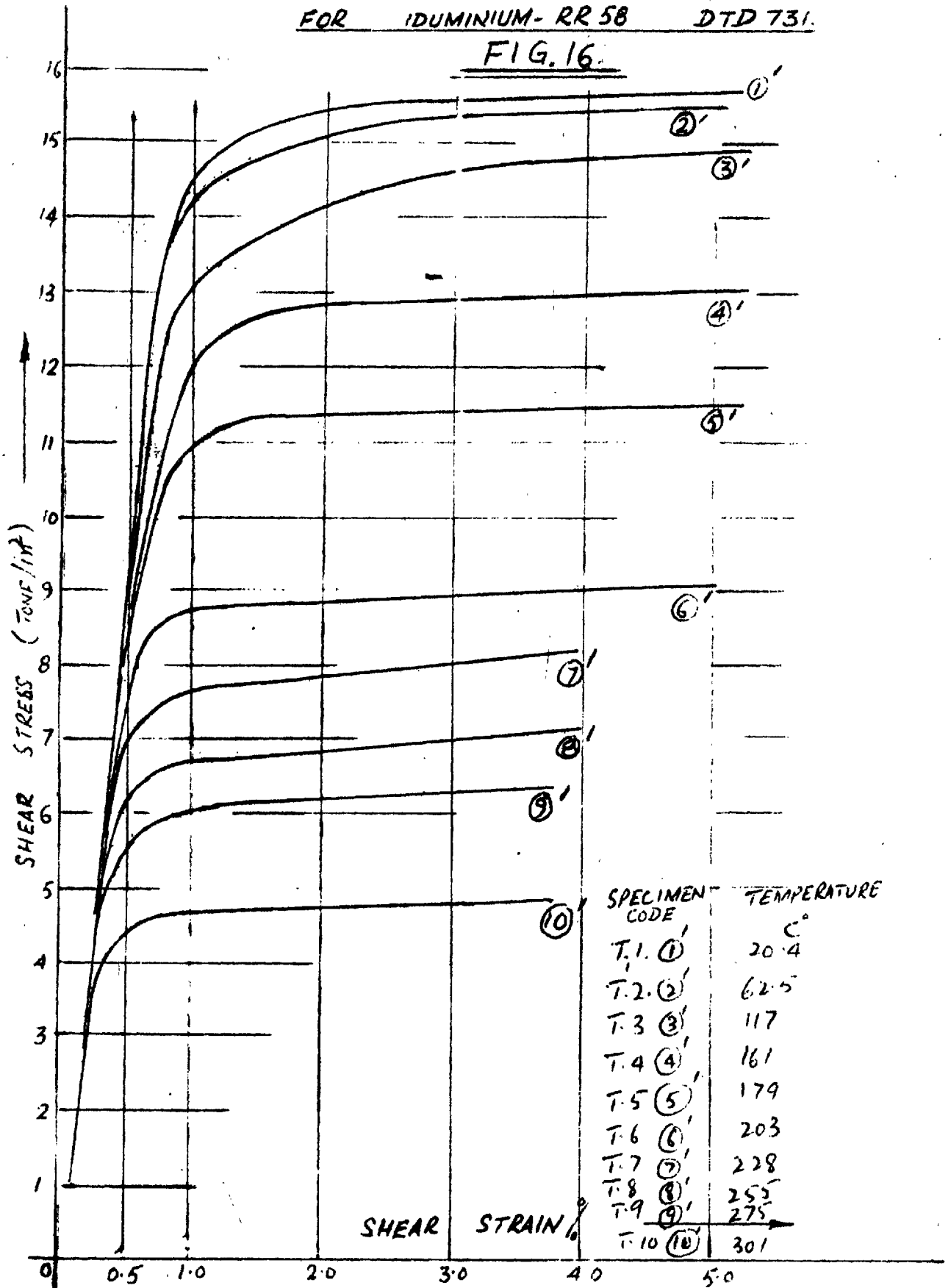
FIG. 15



STATIC TORSION TESTS AT ELEVATED TEMPERATURES.

FOR ALUMINIUM-RR 58 DTD 731.

FIG. 16.

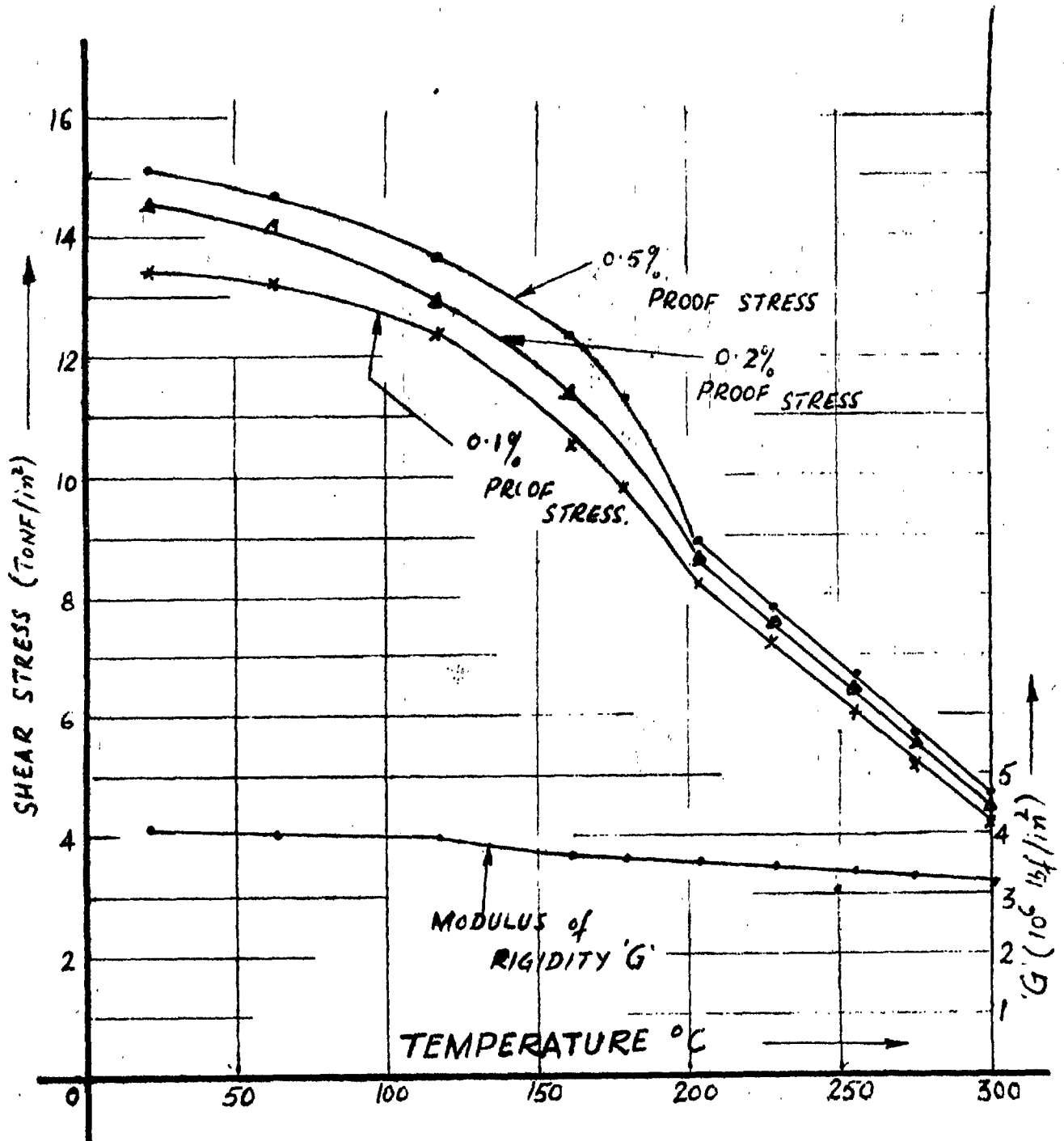


VARIATION OF TORSIONAL PROPERTIES

of ALUMINIUM-RR58 (DTD 731)

AT ELEVATED TEMPERATURES

FIG.17.



CONFIRMATION OF EQUATIONS OF EQUIVALENCE.

(DERIVED IN THE MATHEMATICAL THEORY)

$$(\sigma_x = [3T/T_0]^{1/2} \tau_{xy} \quad \& \quad \epsilon_x = \gamma_{xy} [1/3 T/T_0]^{1/2})$$

BASED ON EXPERIMENTAL DATA SHOWN
IN FIGS. 12 to 17

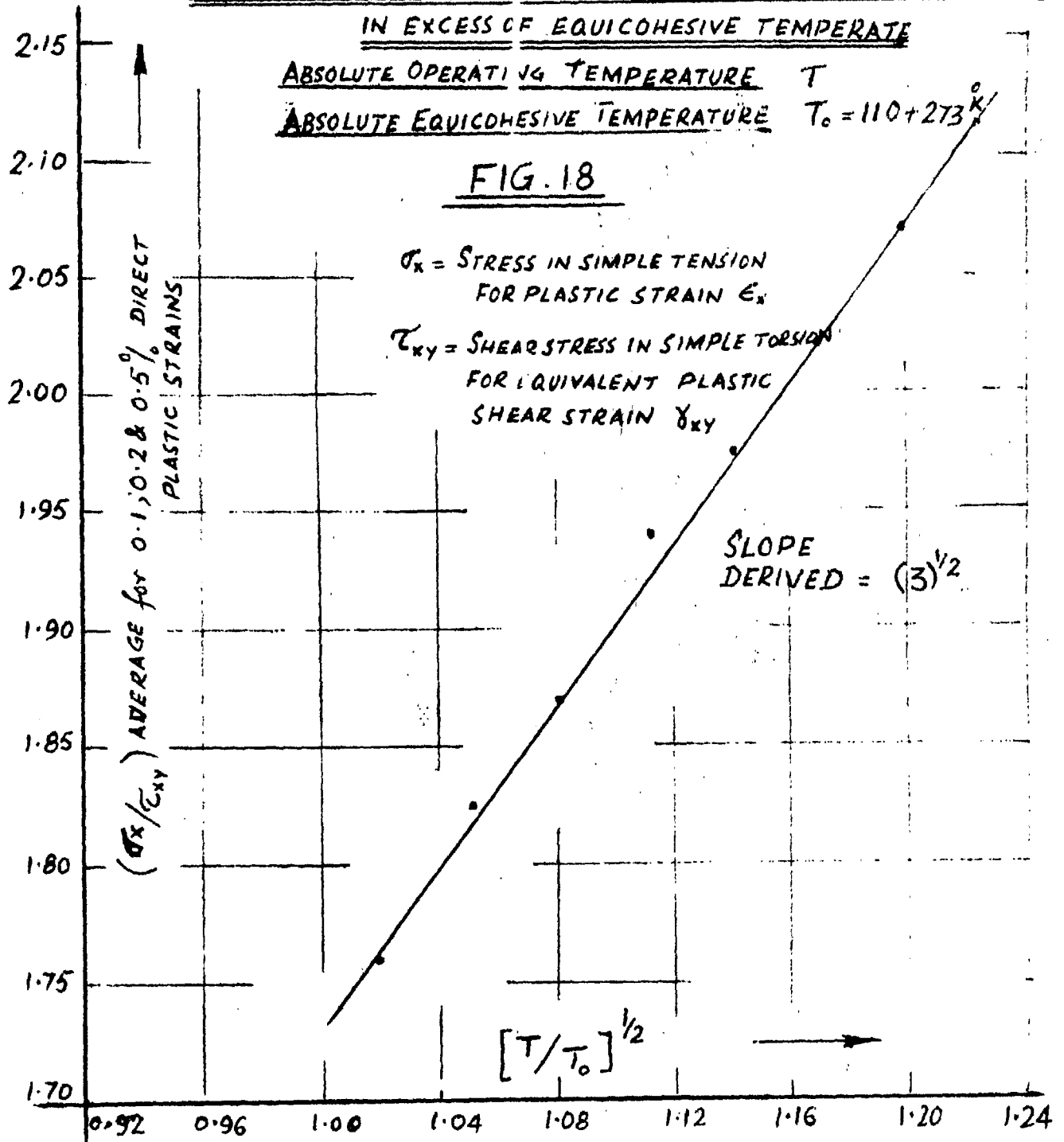
FOR HYDUMINIUM-RR-8 (DTD731) AT ELEVATED TEMPERATURES

IN EXCESS OF EQUICOHESIVE TEMPERATURE

ABSOLUTE OPERATING TEMPERATURE T

ABSOLUTE EQUICOHESIVE TEMPERATURE T₀ = 110 + 273°K

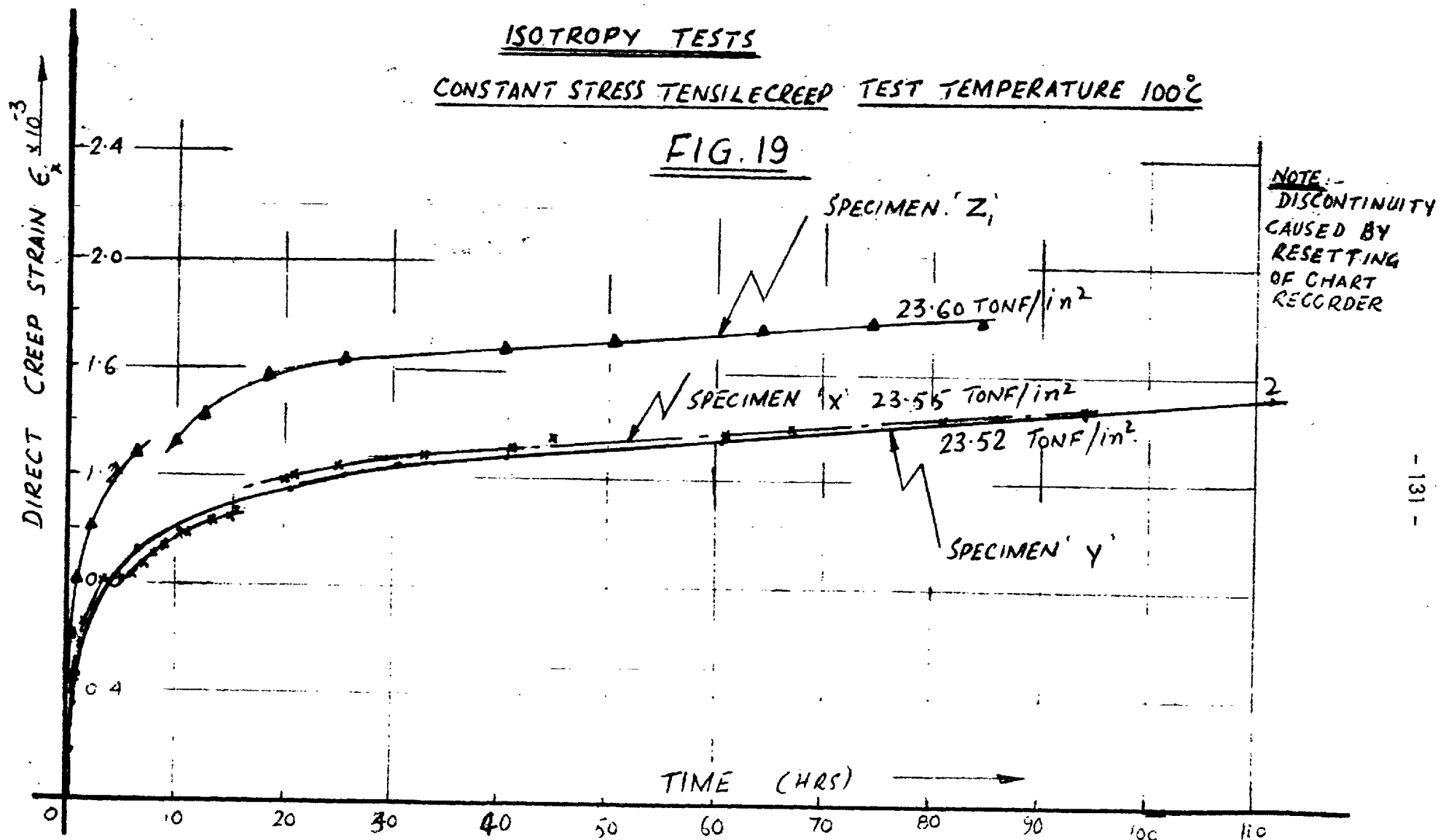
FIG. 18



ISOTROPY TESTS

CONSTANT STRESS TENSILE CREEP TEST TEMPERATURE 100°C

FIG. 19

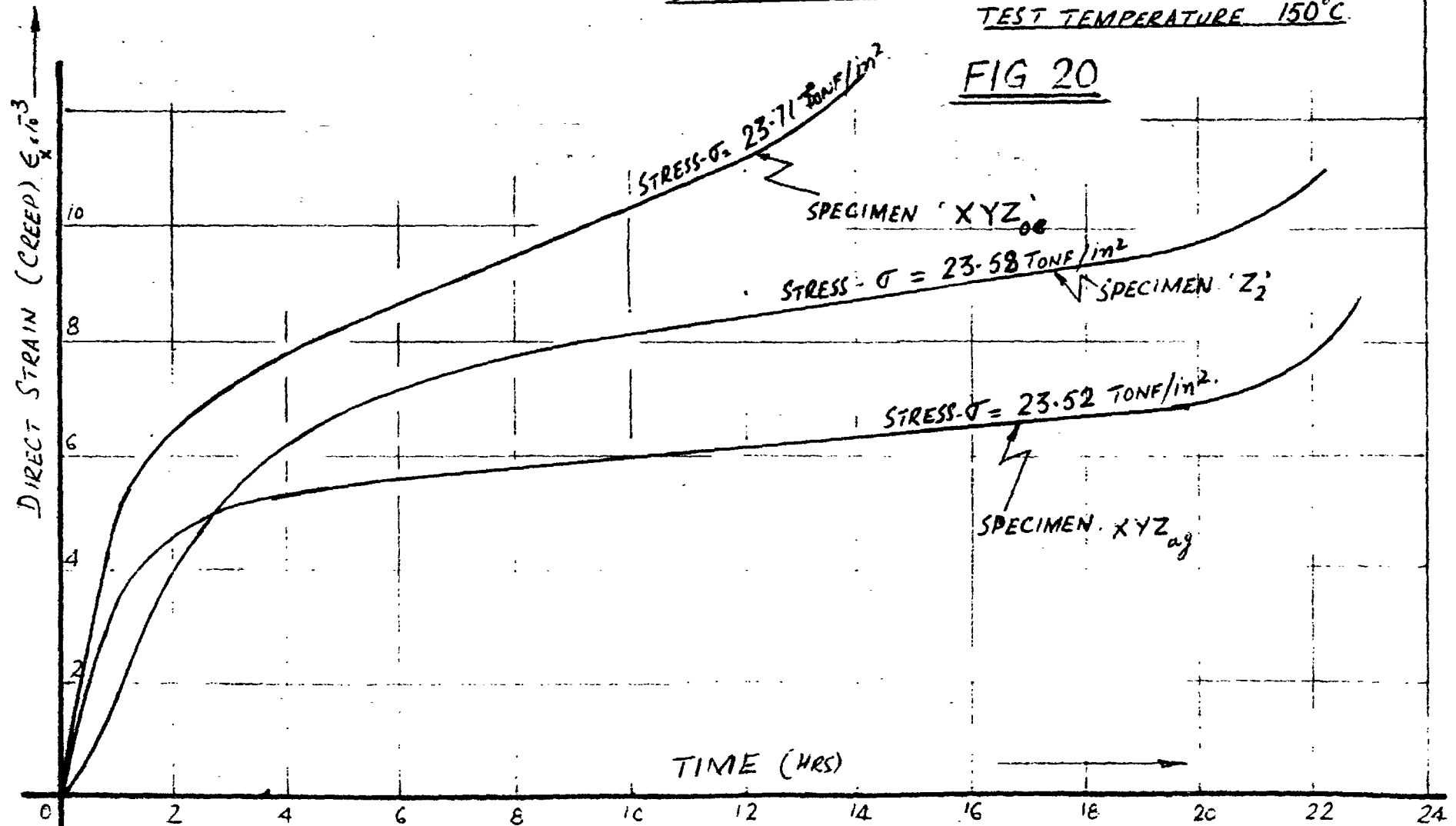


ISOTROPY TESTS

CONSTANT STRESS TENSILE CREEP

TEST TEMPERATURE 150°C.

FIG 20

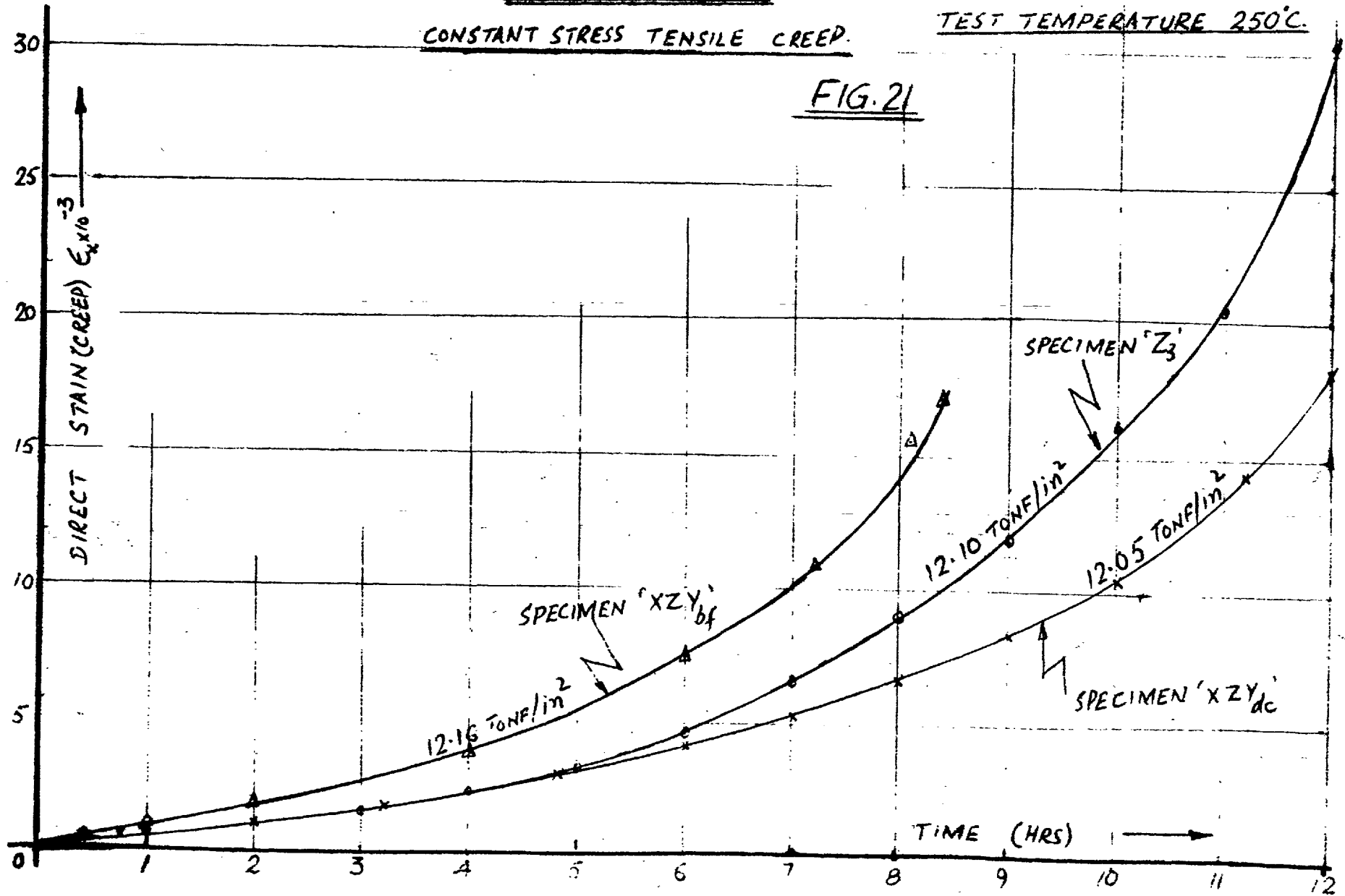


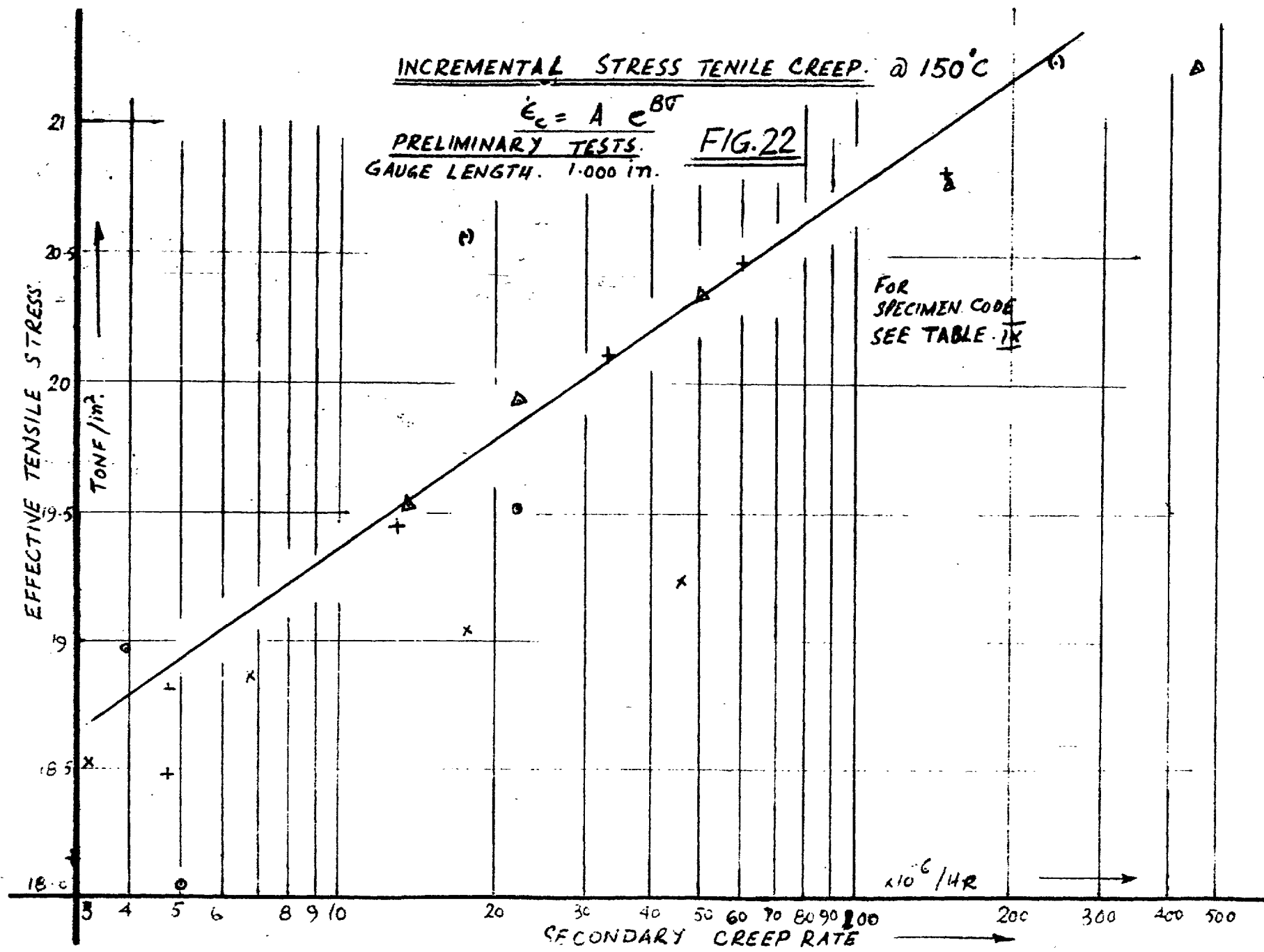
ISOTROPY TESTS

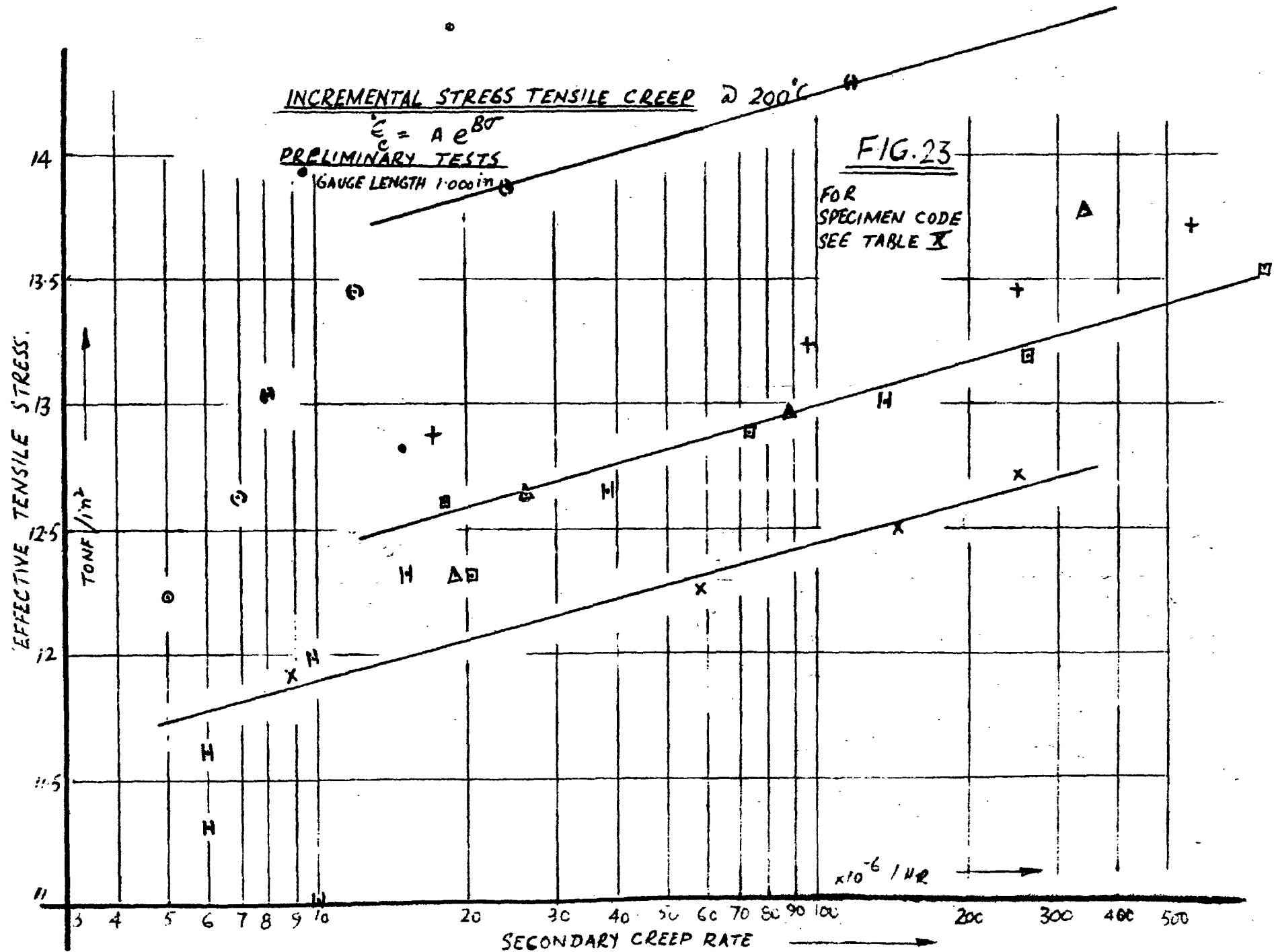
CONSTANT STRESS TENSILE CREEP.

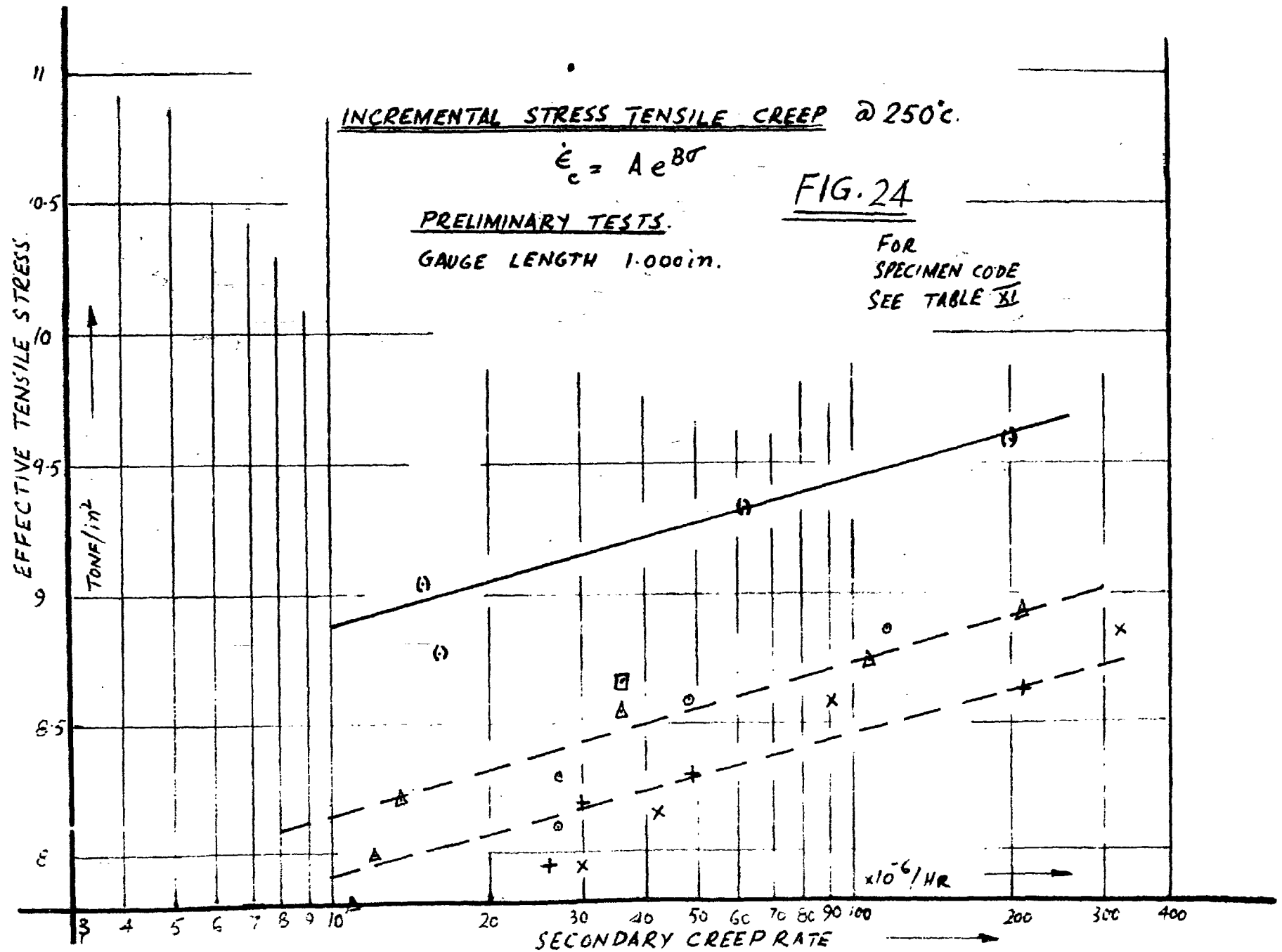
TEST TEMPERATURE 250°C.

FIG. 21









INCREMENTAL STRESS TENSILE CREEP @ 150°C.

$(U_{SH} + W_P)$ vs $\sigma_{e_i} \dot{\epsilon}_{e_i}$

PRELIMINARY TESTS.

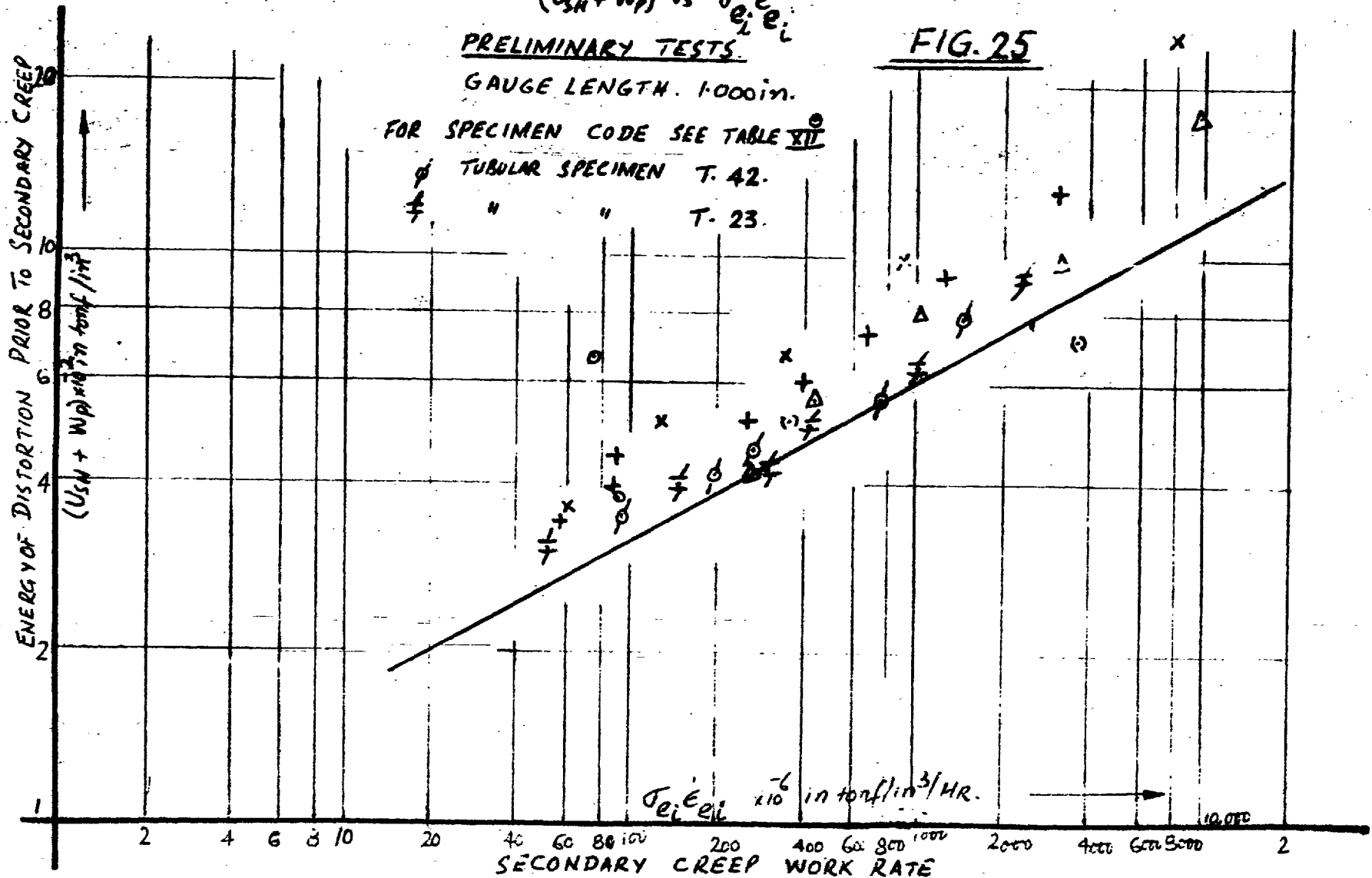
GAUGE LENGTH. 1.000 in.

FOR SPECIMEN CODE SEE TABLE XII

○ TUBULAR SPECIMEN T. 42.

+ " " T. 23.

FIG. 25



INCREMENTAL STRESS TENSILE CREEP @ 200°C.

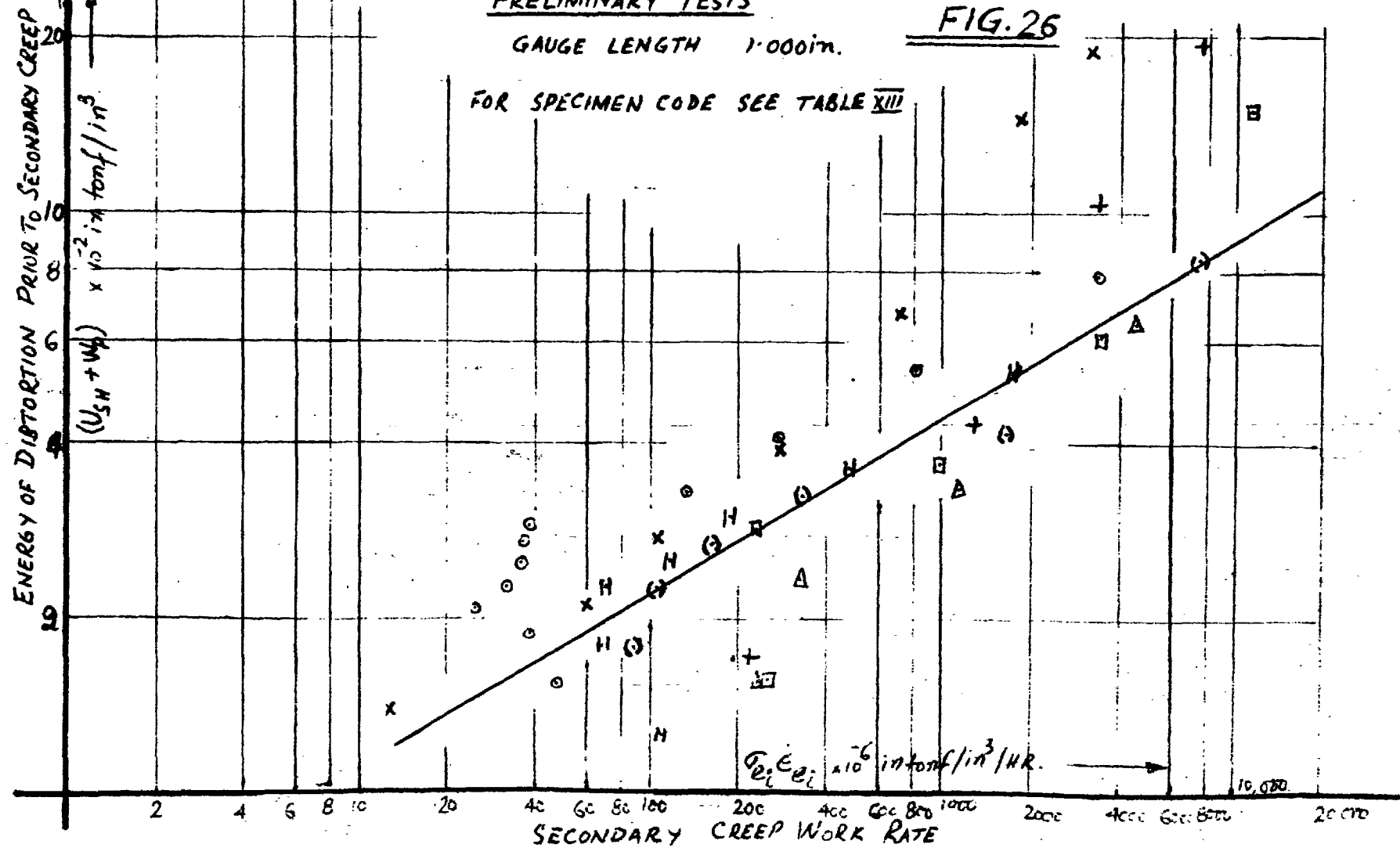
$(U_{SH} + W_p)$ vs $\sigma_{ci} \dot{\epsilon}_{ci}$

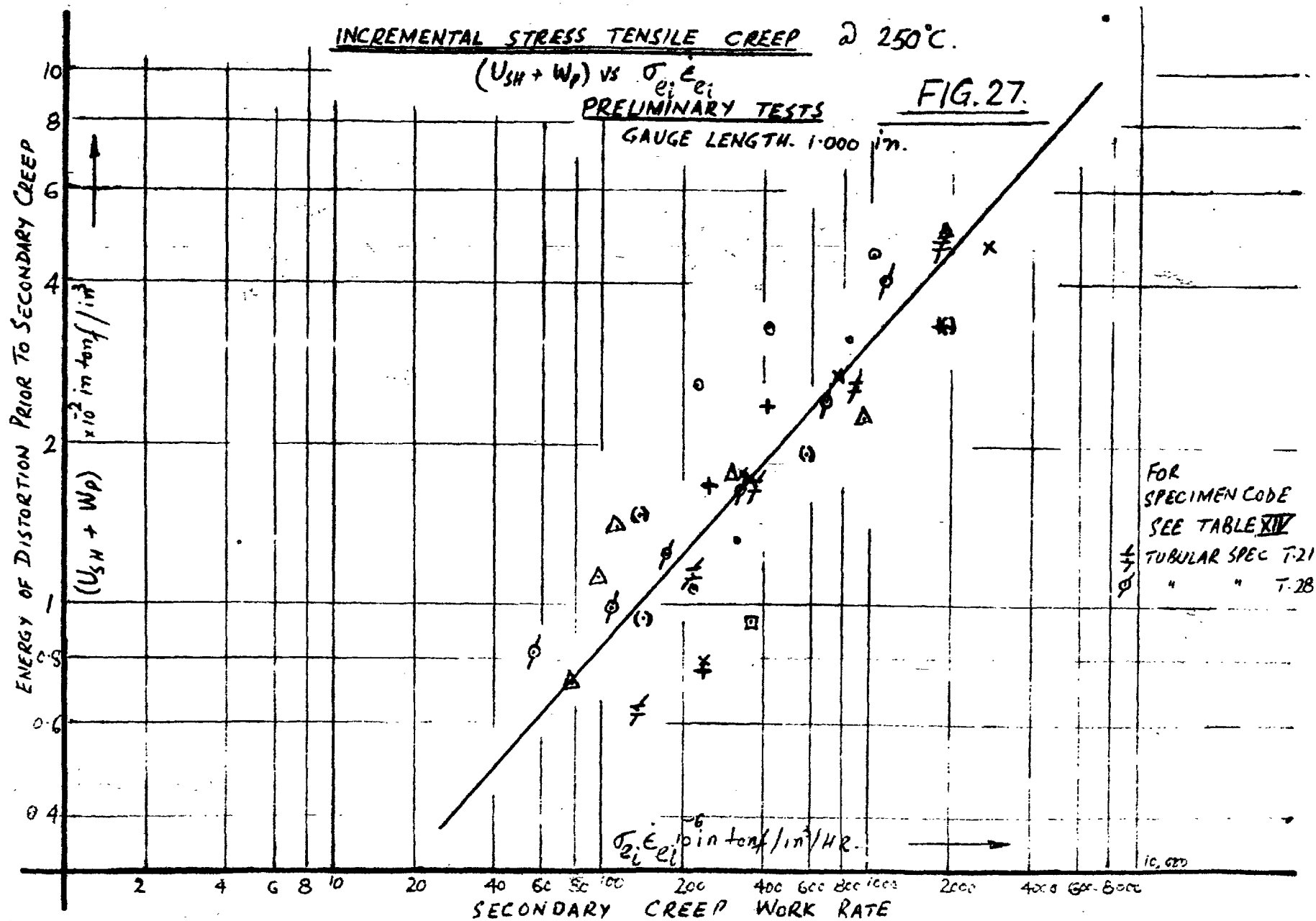
PRELIMINARY TESTS

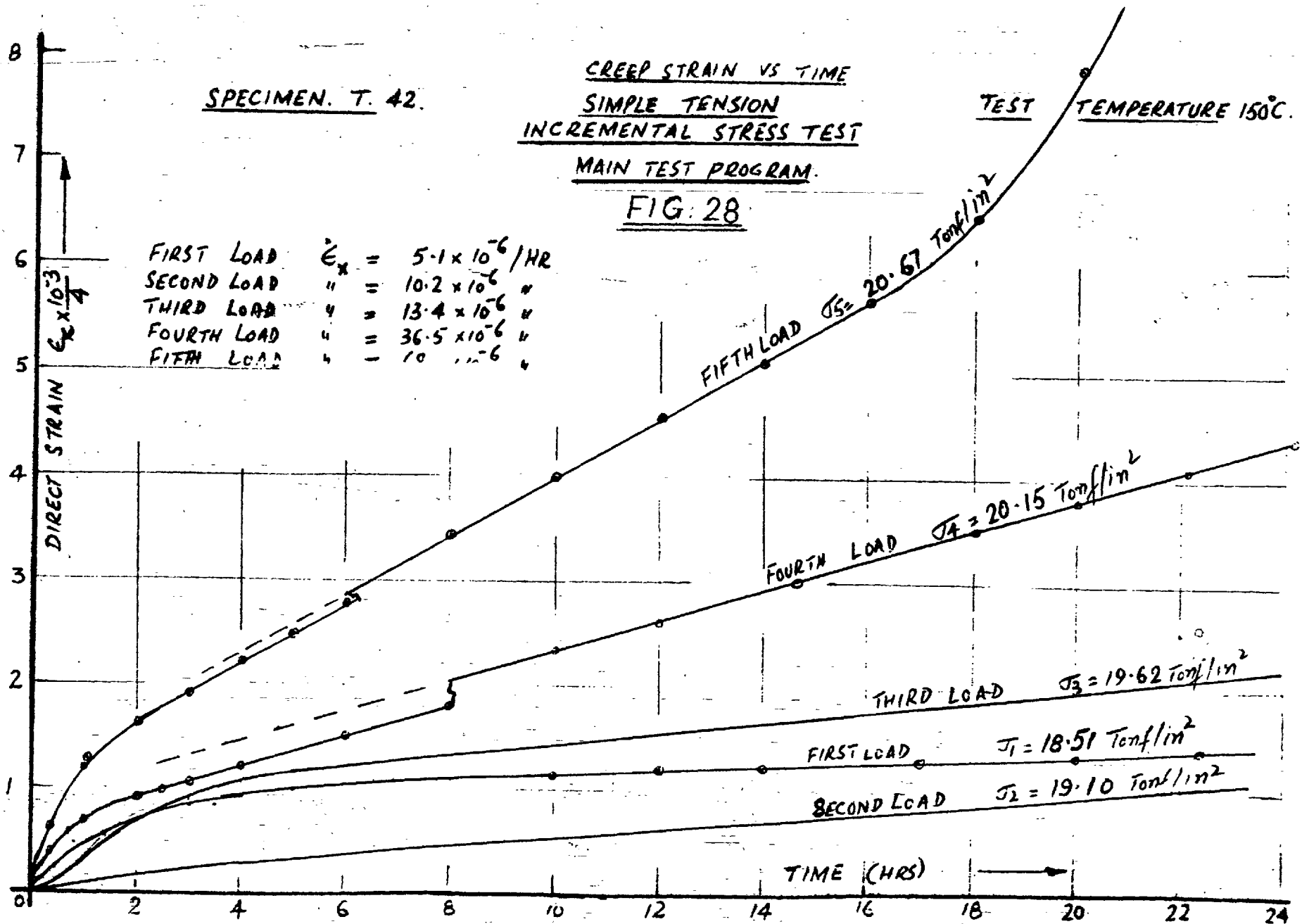
GAUGE LENGTH 1.000in.

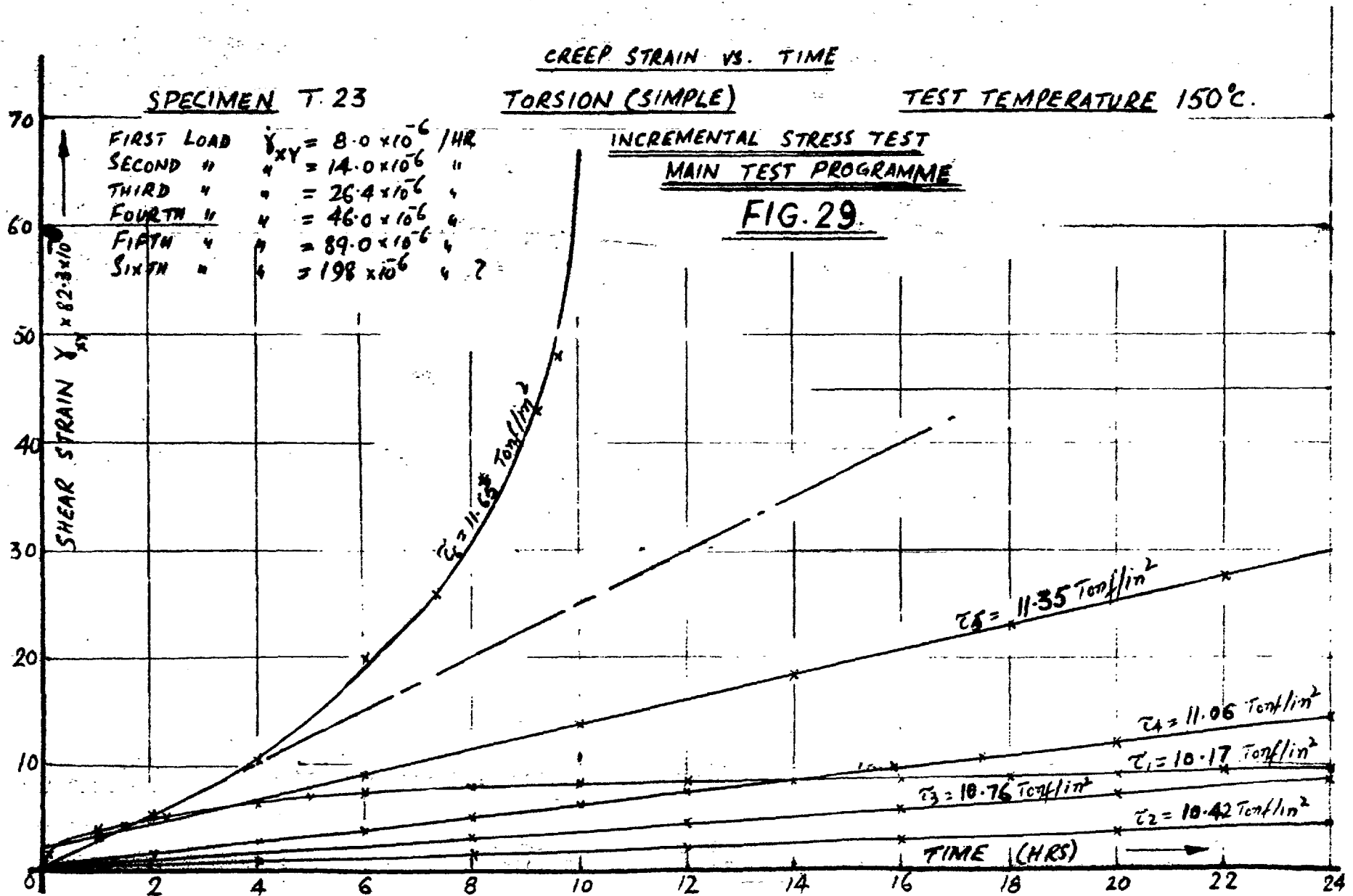
FIG. 26

FOR SPECIMEN CODE SEE TABLE XIII









CREEP STRAIN VS. TIME

SPECIMEN. T.37.

COMBINED LOADS
(TENSION/TORSION)

TEST TEMPERATURE 150°C

INCREMENTAL STRESS TEST

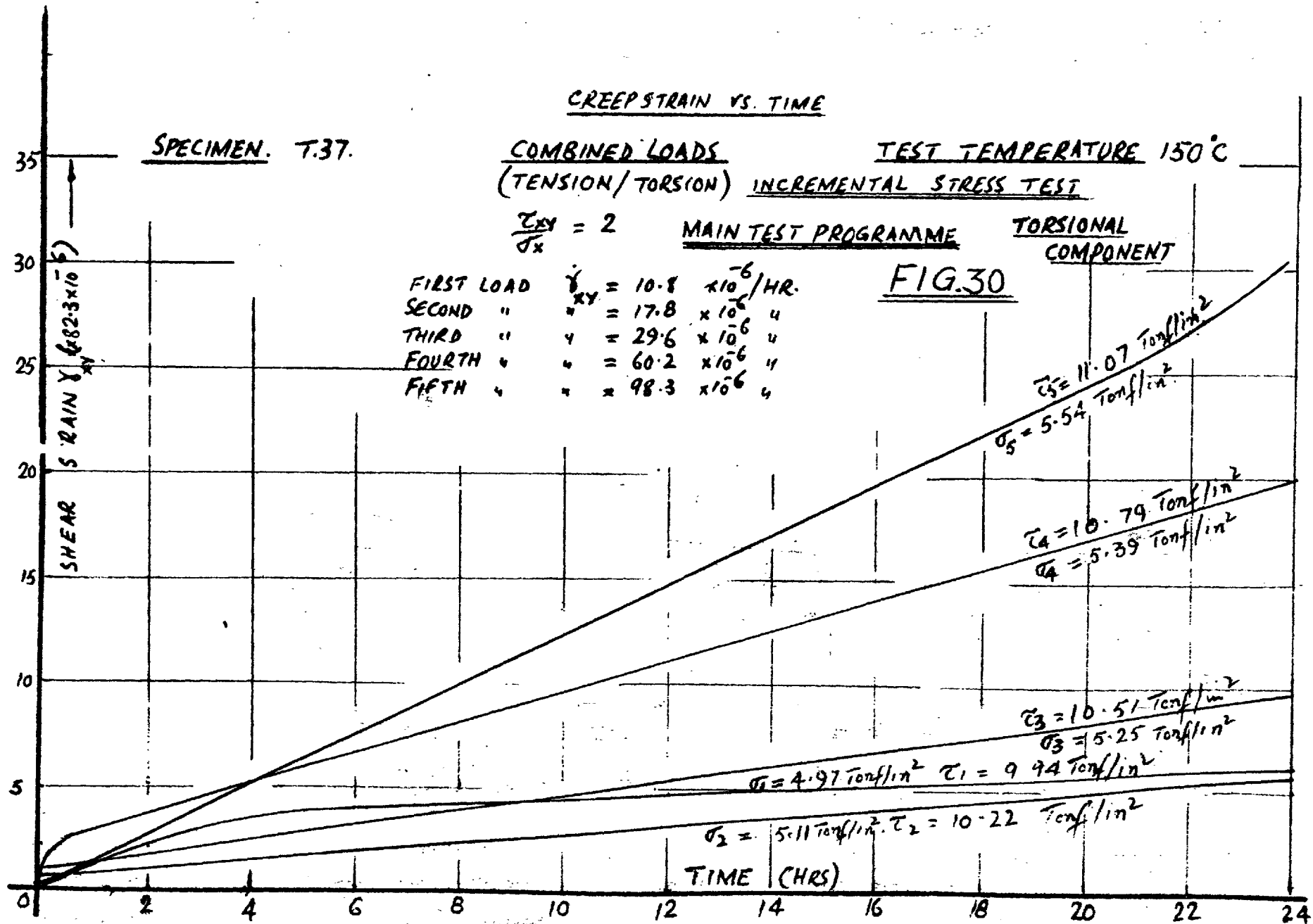
$$\frac{\tau_{xy}}{\sigma_x} = 2$$

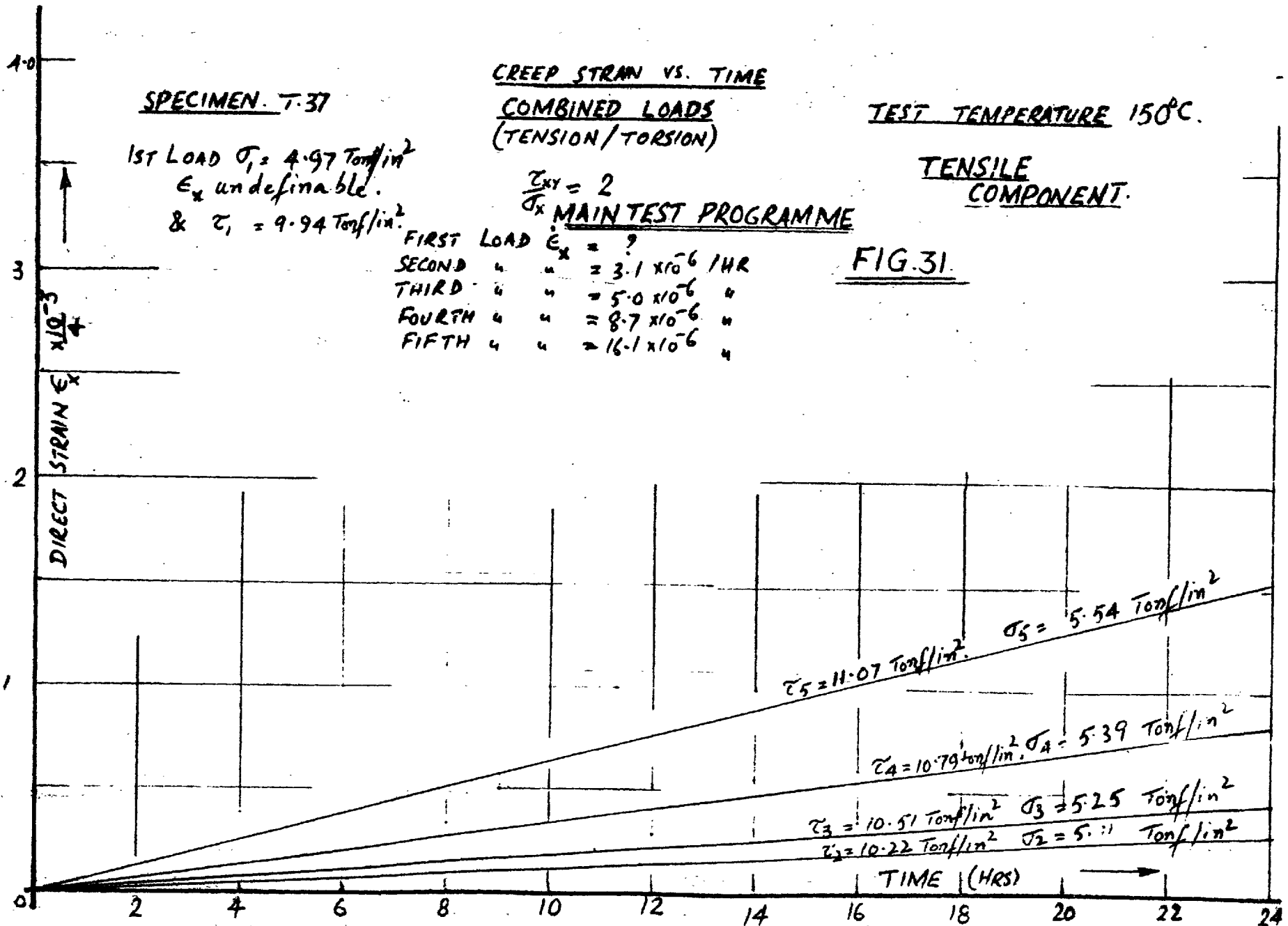
MAIN TEST PROGRAMME

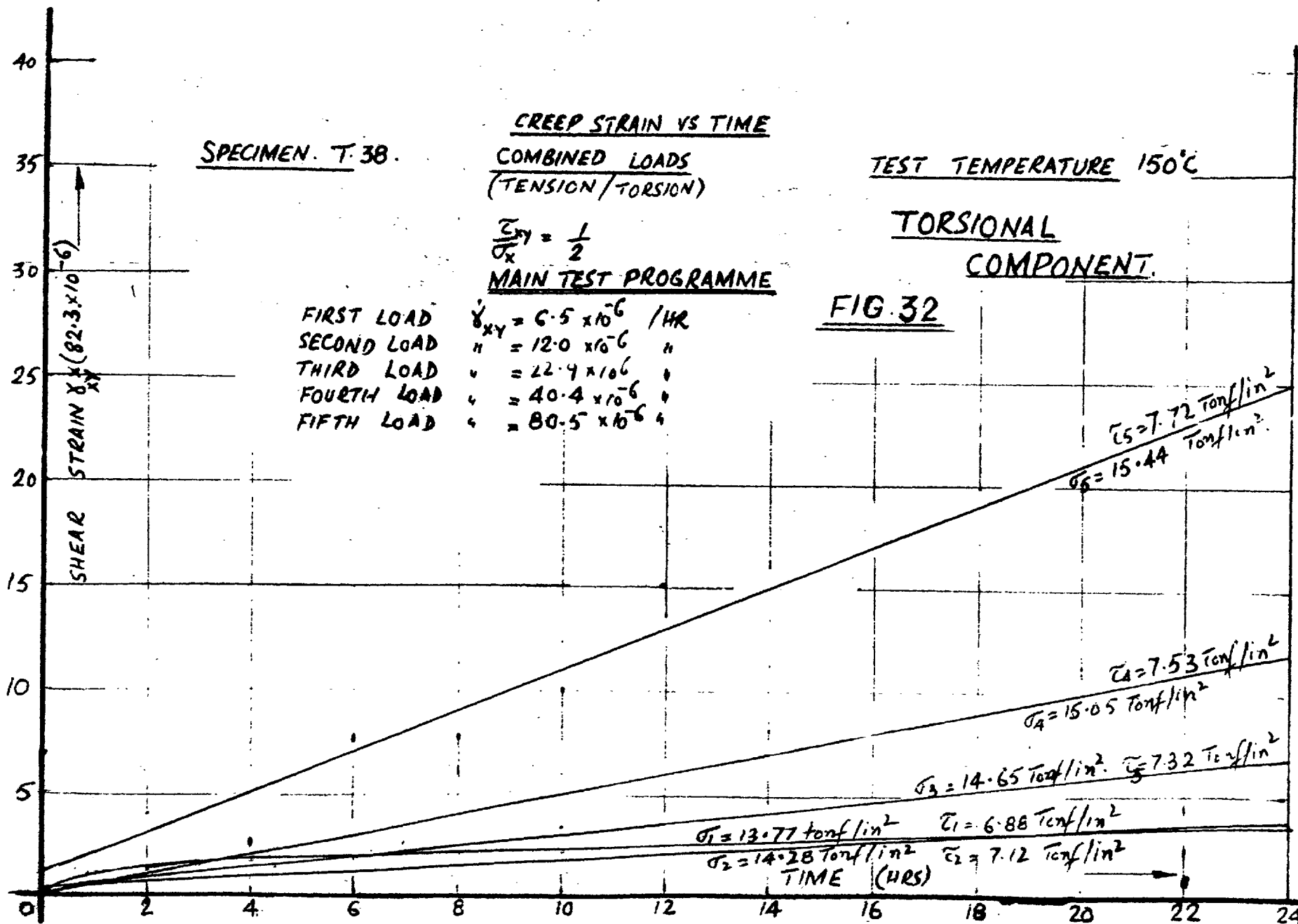
TORSIONAL COMPONENT

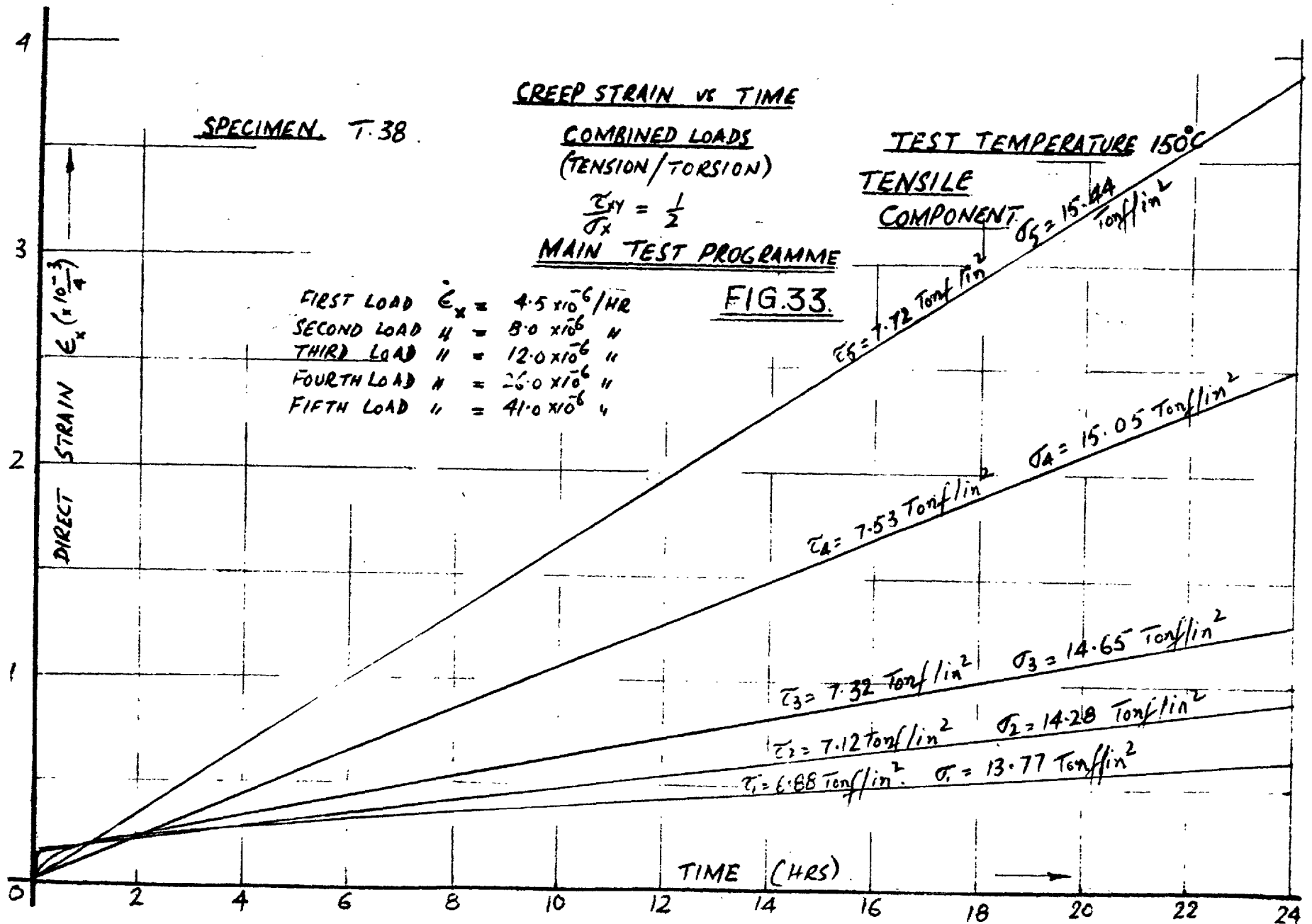
FIG.30

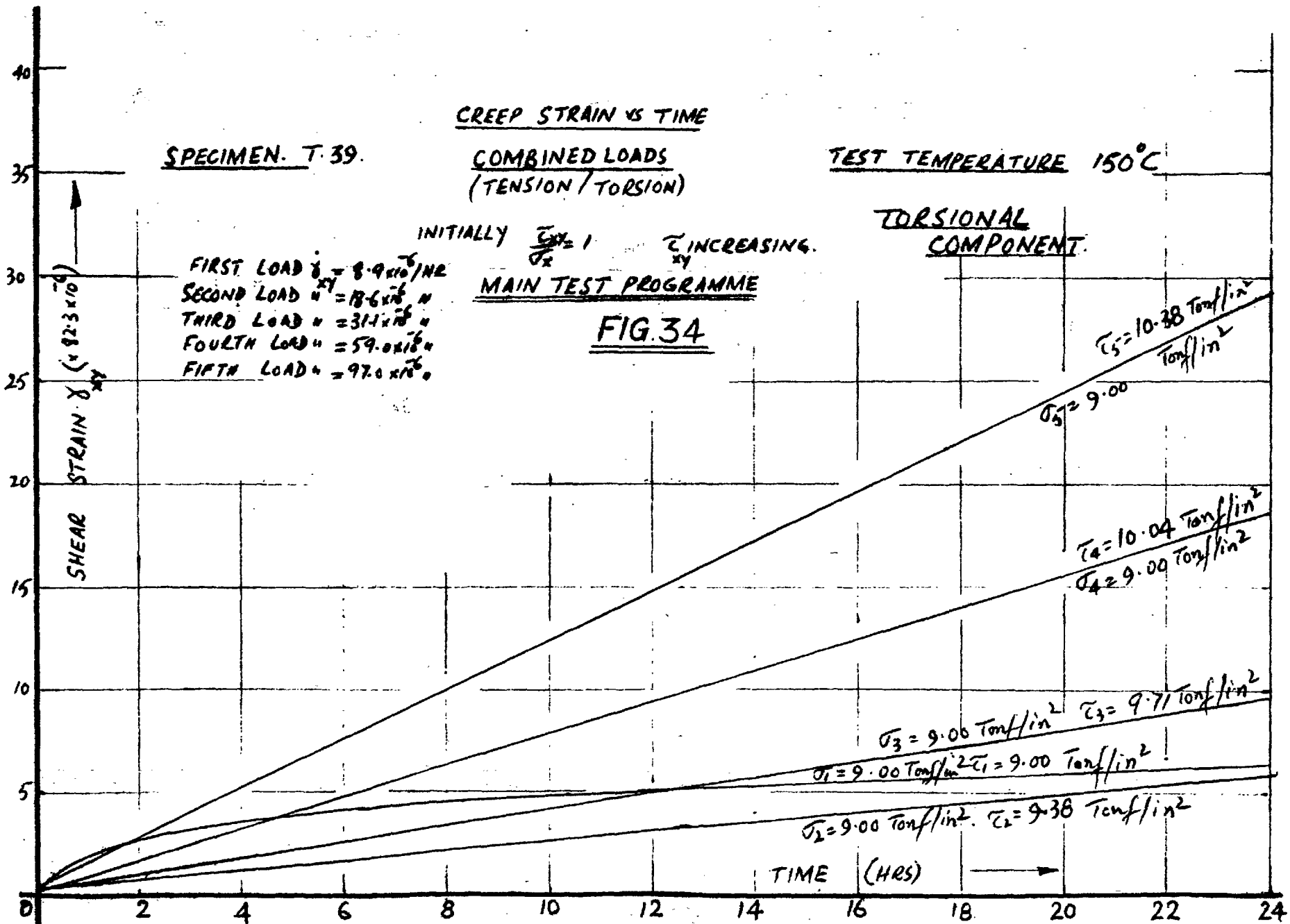
FIRST LOAD	$\dot{\gamma}_{xy} = 10.8 \times 10^{-6} / \text{HR.}$
SECOND "	$\dot{\gamma}_{xy} = 17.8 \times 10^{-6} "$
THIRD "	$\dot{\gamma}_{xy} = 29.6 \times 10^{-6} "$
FOURTH "	$\dot{\gamma}_{xy} = 60.2 \times 10^{-6} "$
FIFTH "	$\dot{\gamma}_{xy} = 98.3 \times 10^{-6} "$

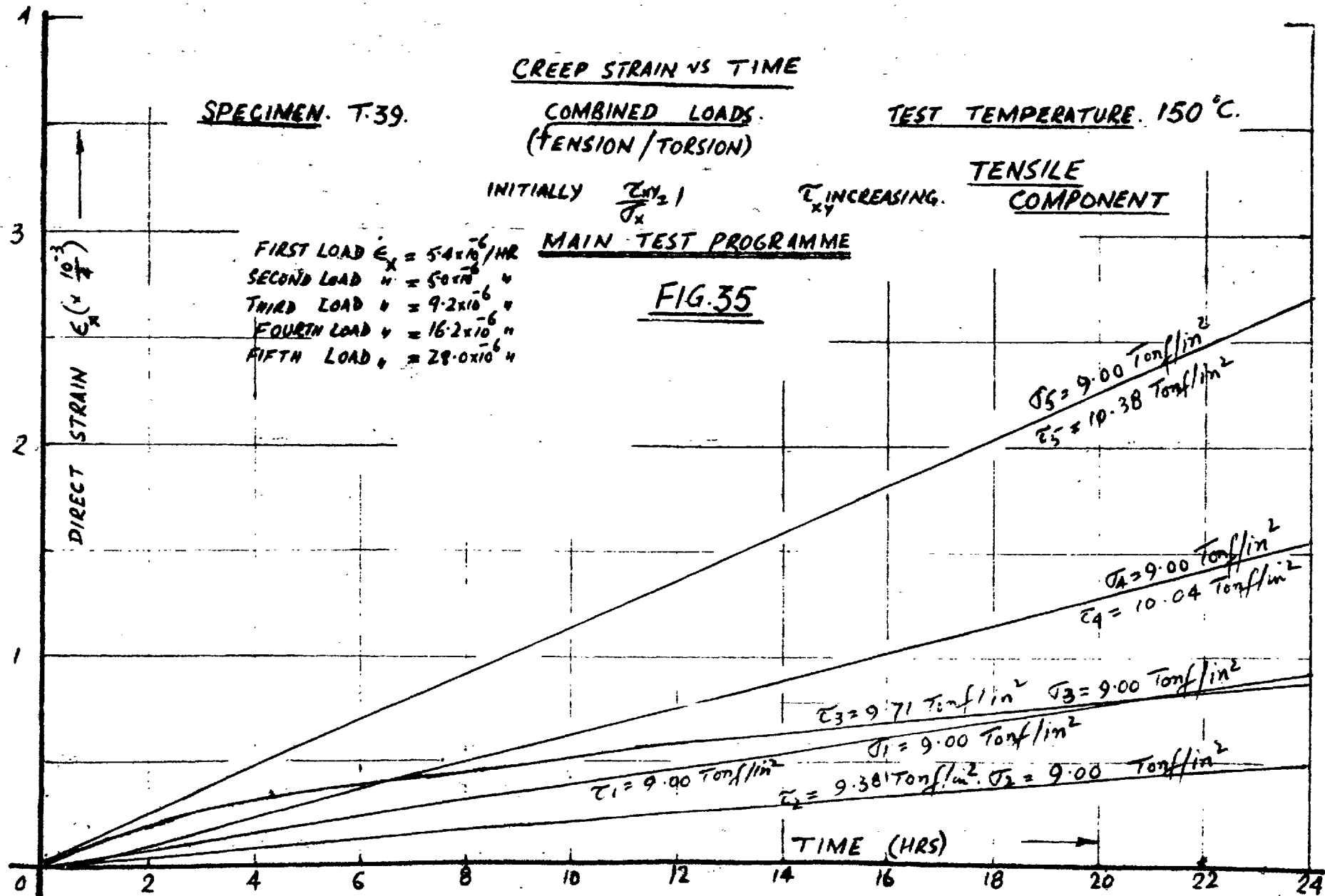


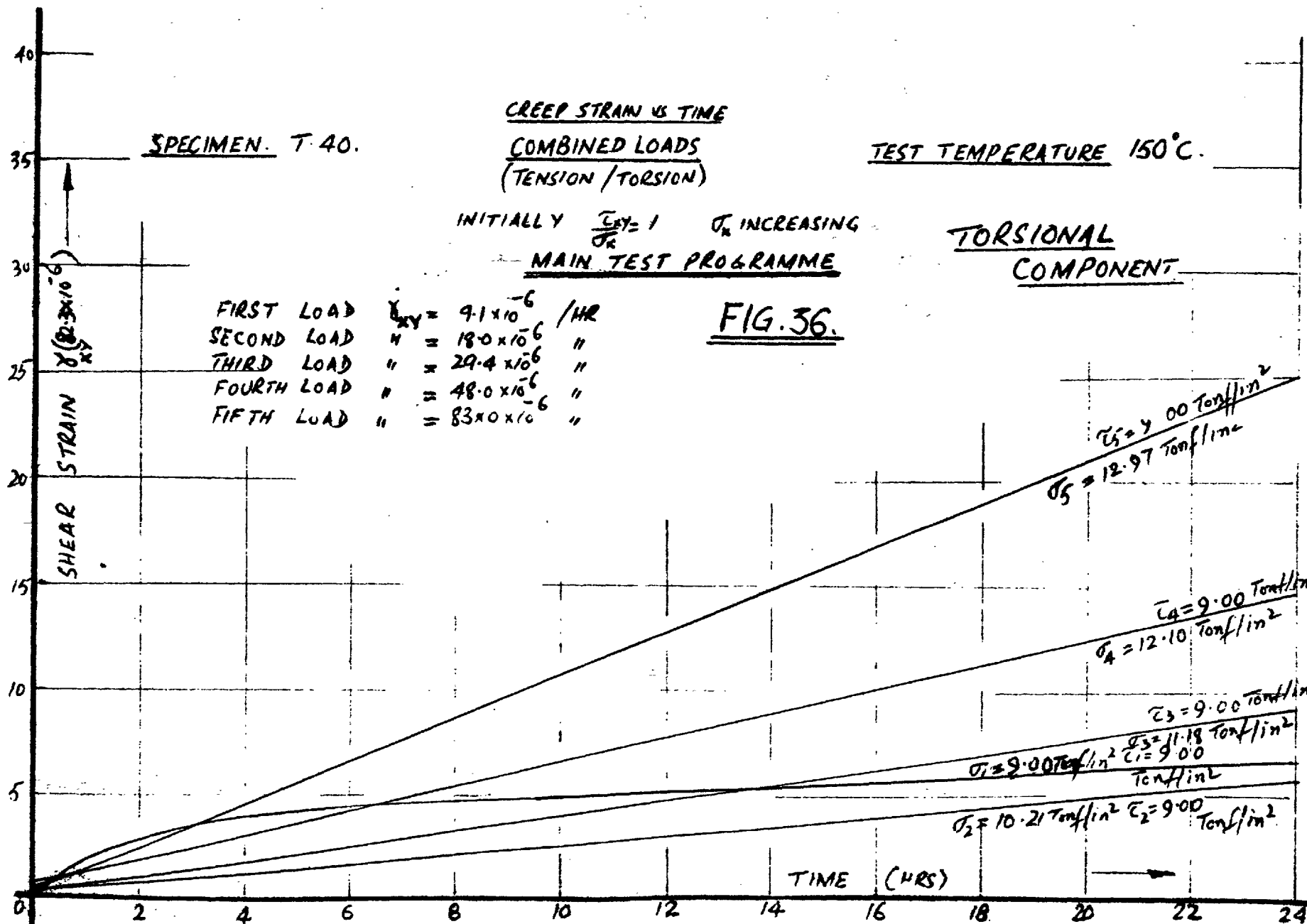


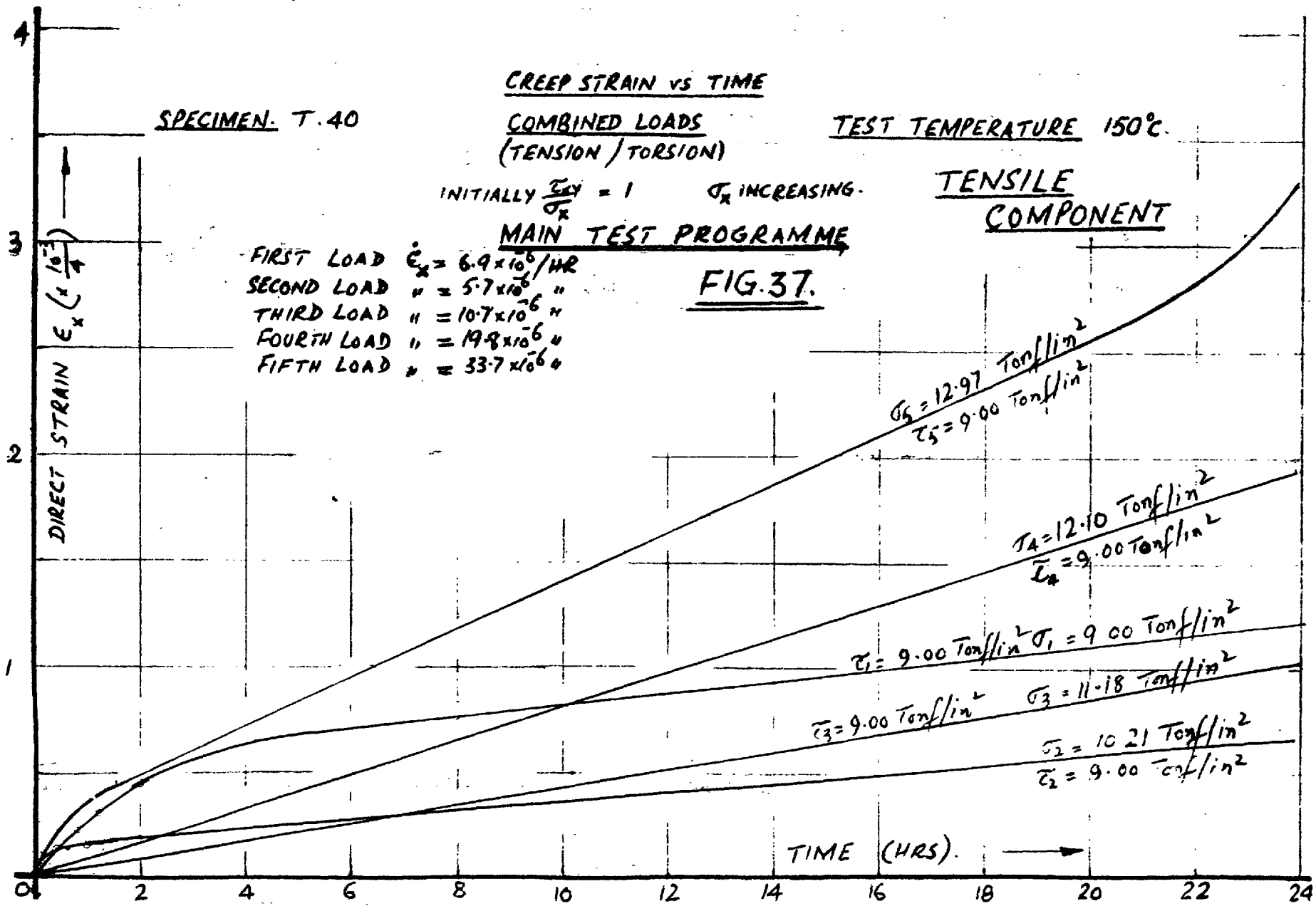


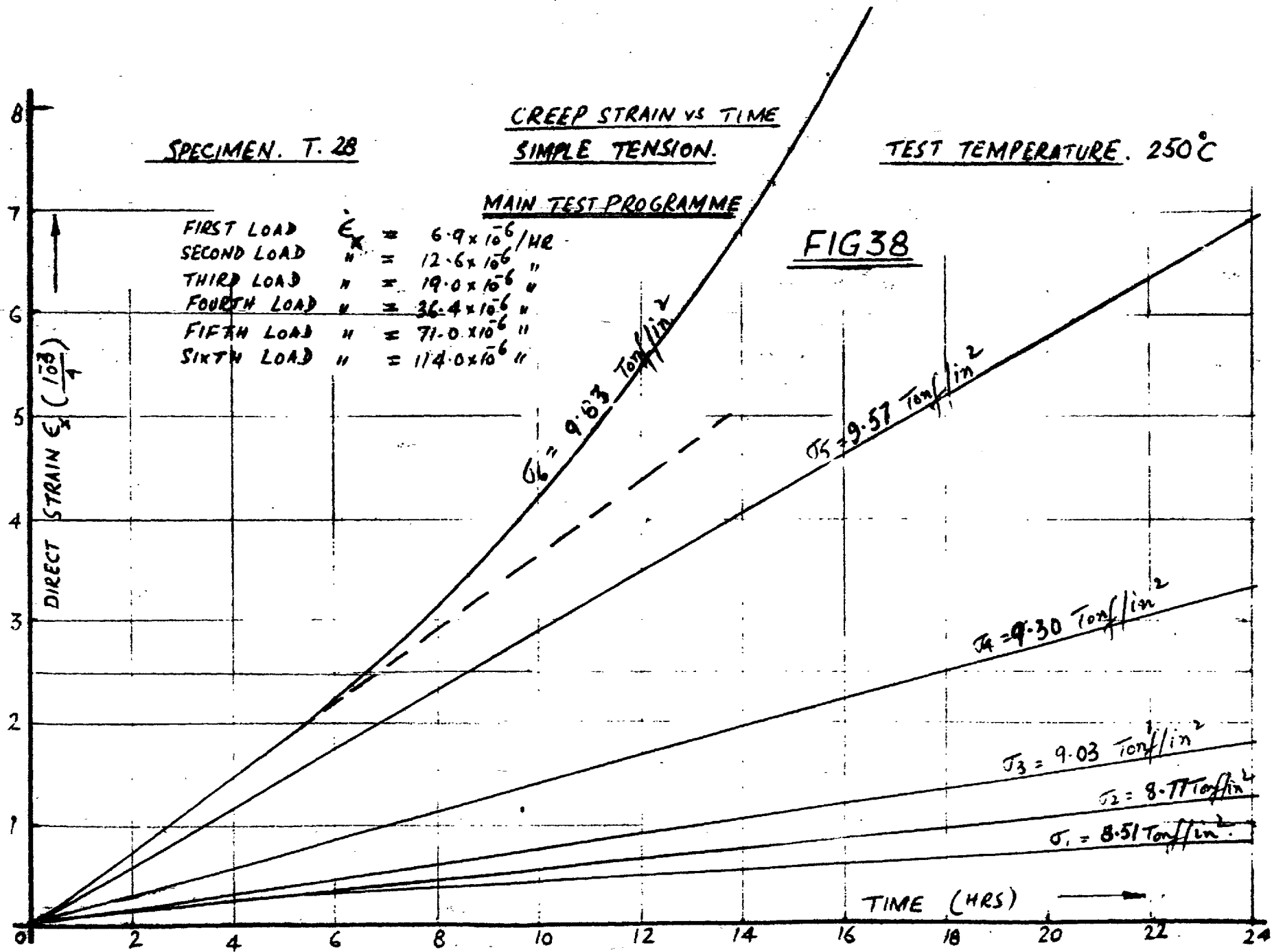


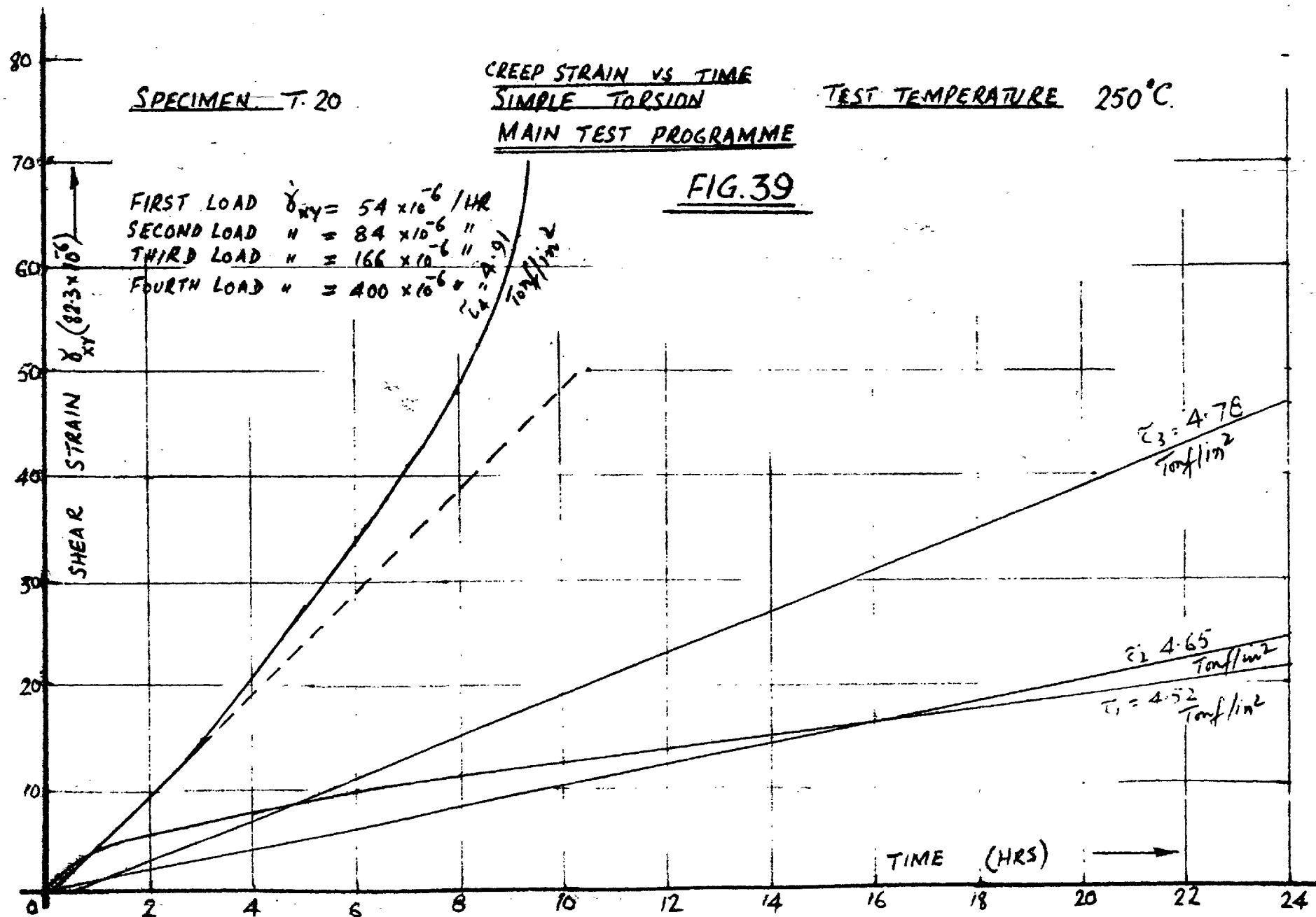


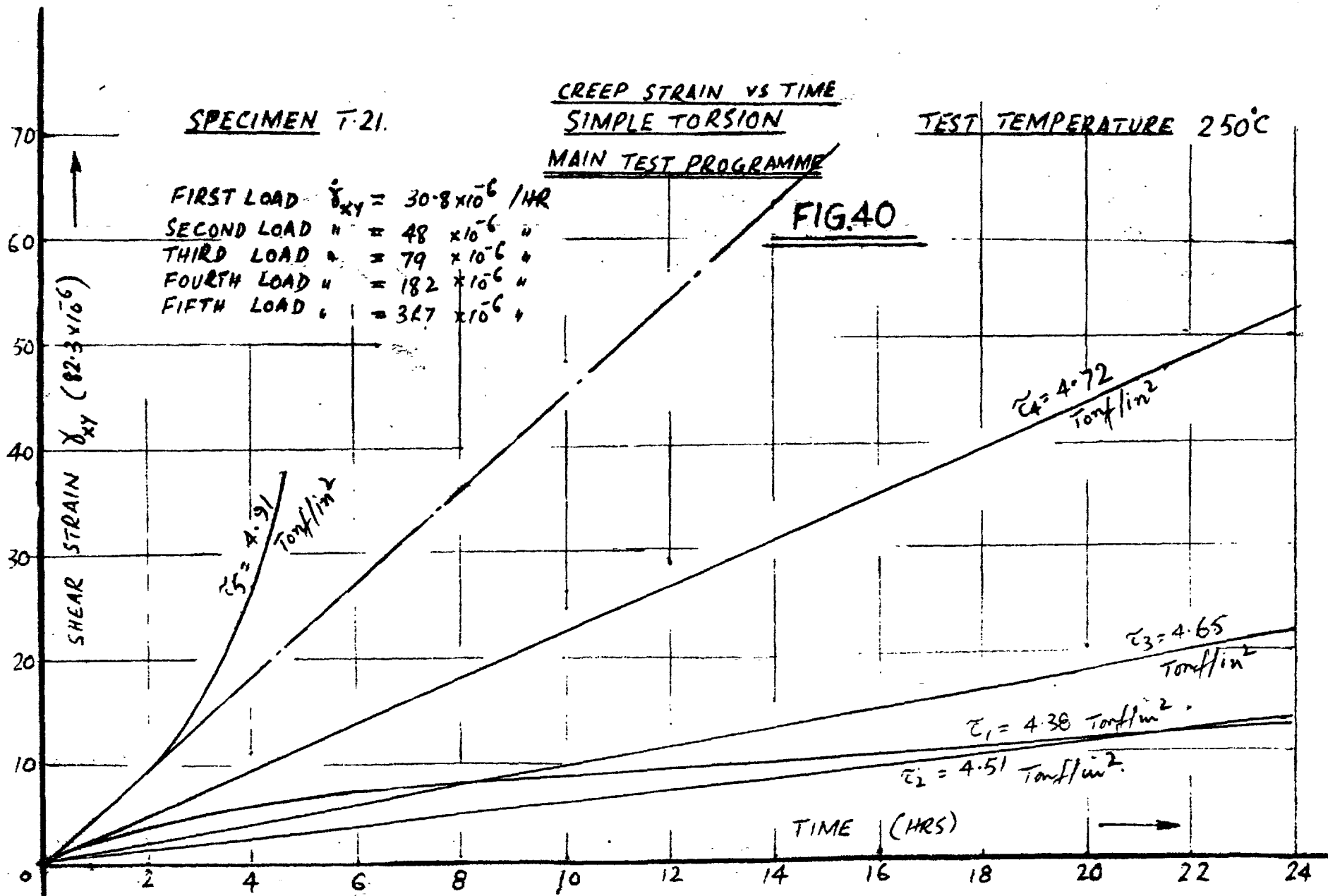


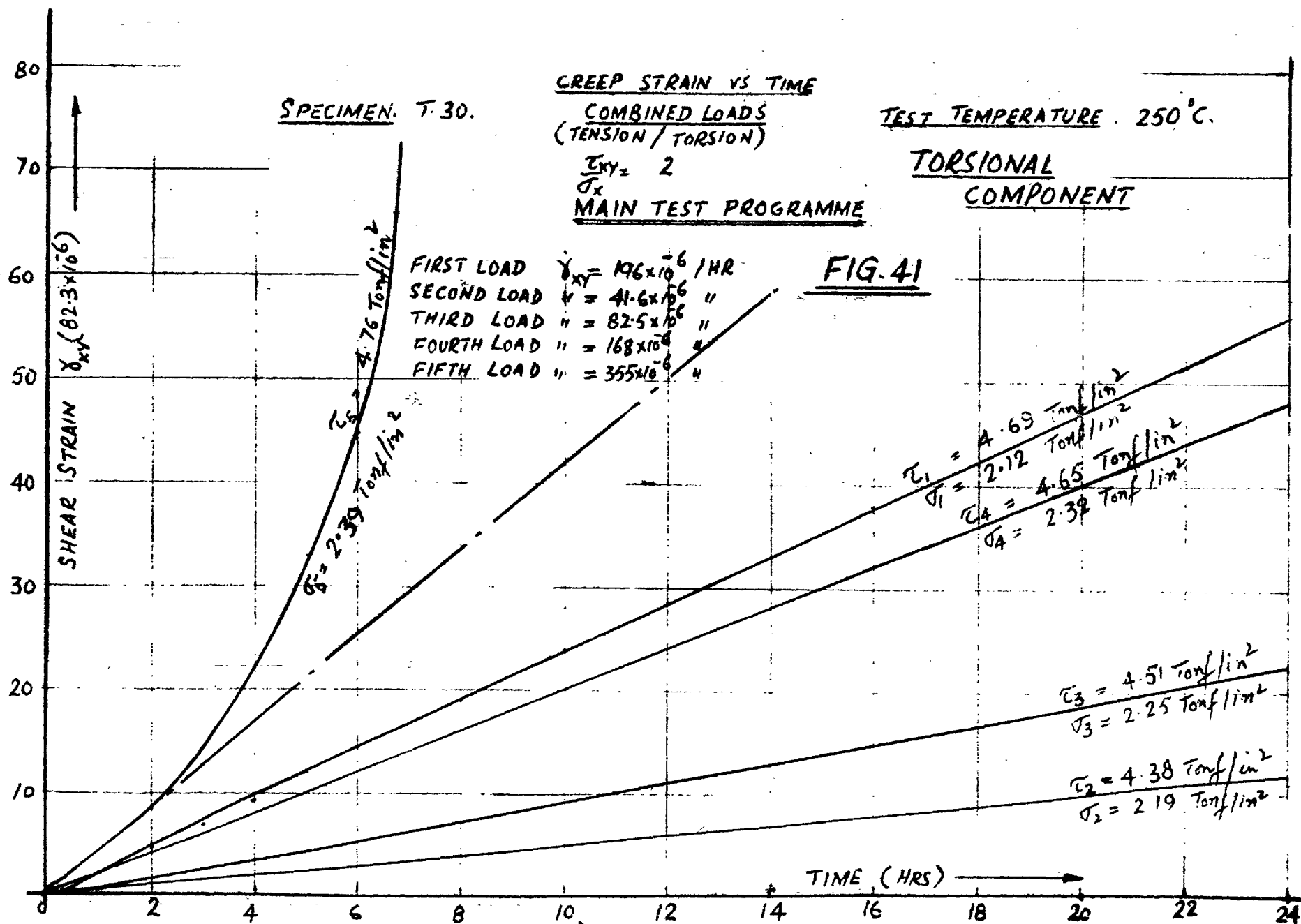












SPECIMEN. T. 30

CREEP STRAIN VS TIME

COMBINED LOADS
(TENSION / TORSION)

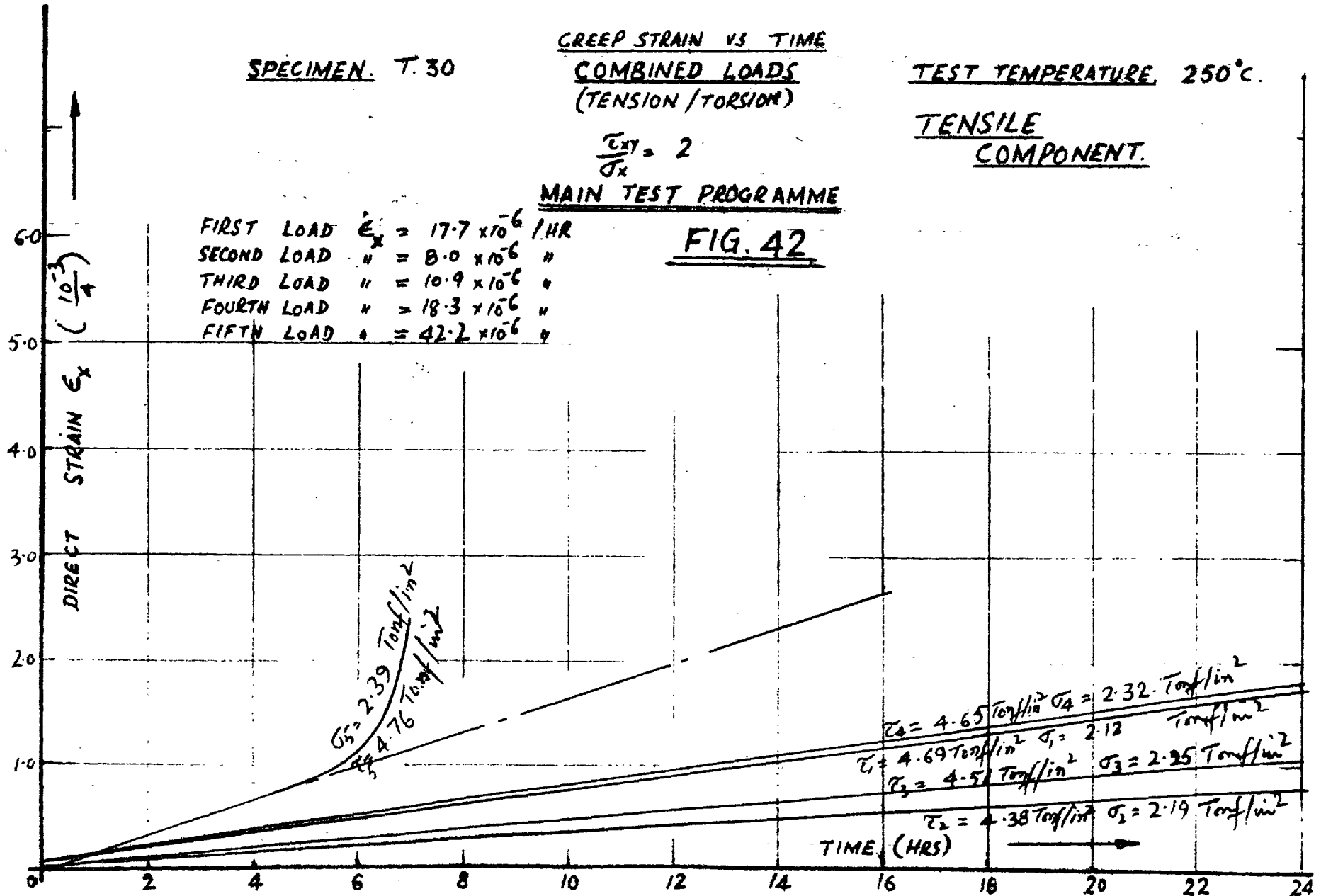
TEST TEMPERATURE 250°C.

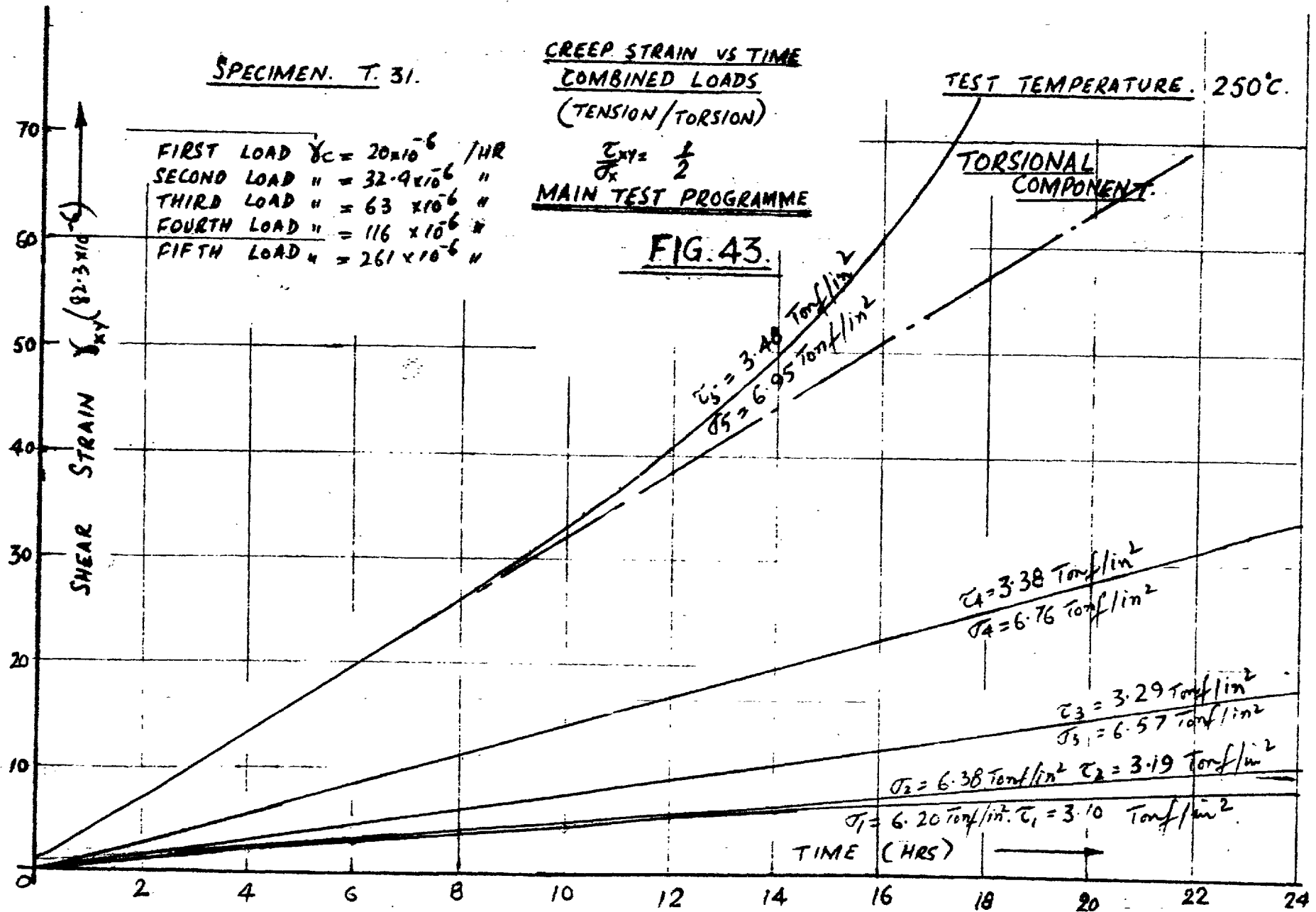
$$\frac{\tau_{xy}}{\sigma_x} = 2$$

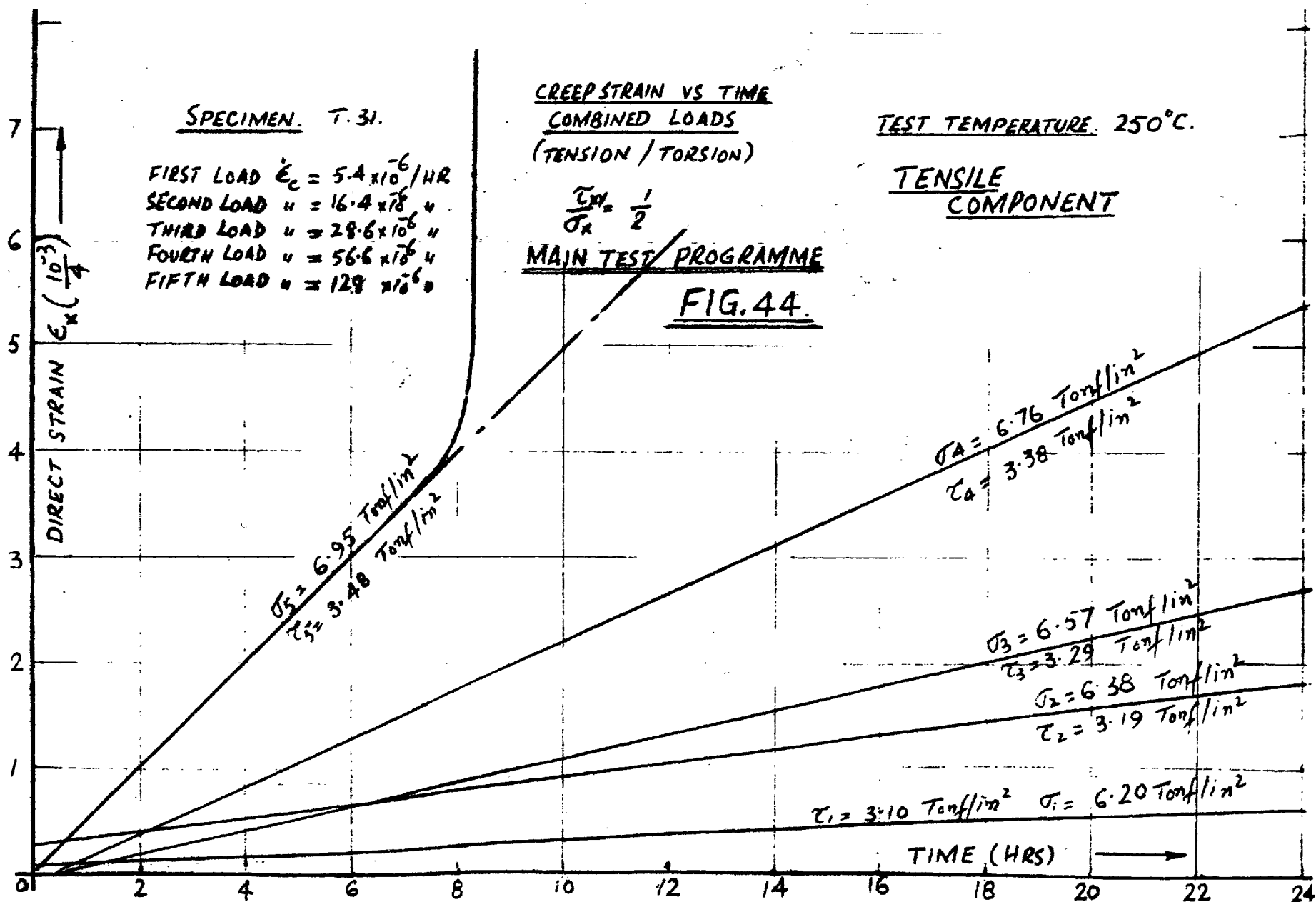
TENSILE
COMPONENT.

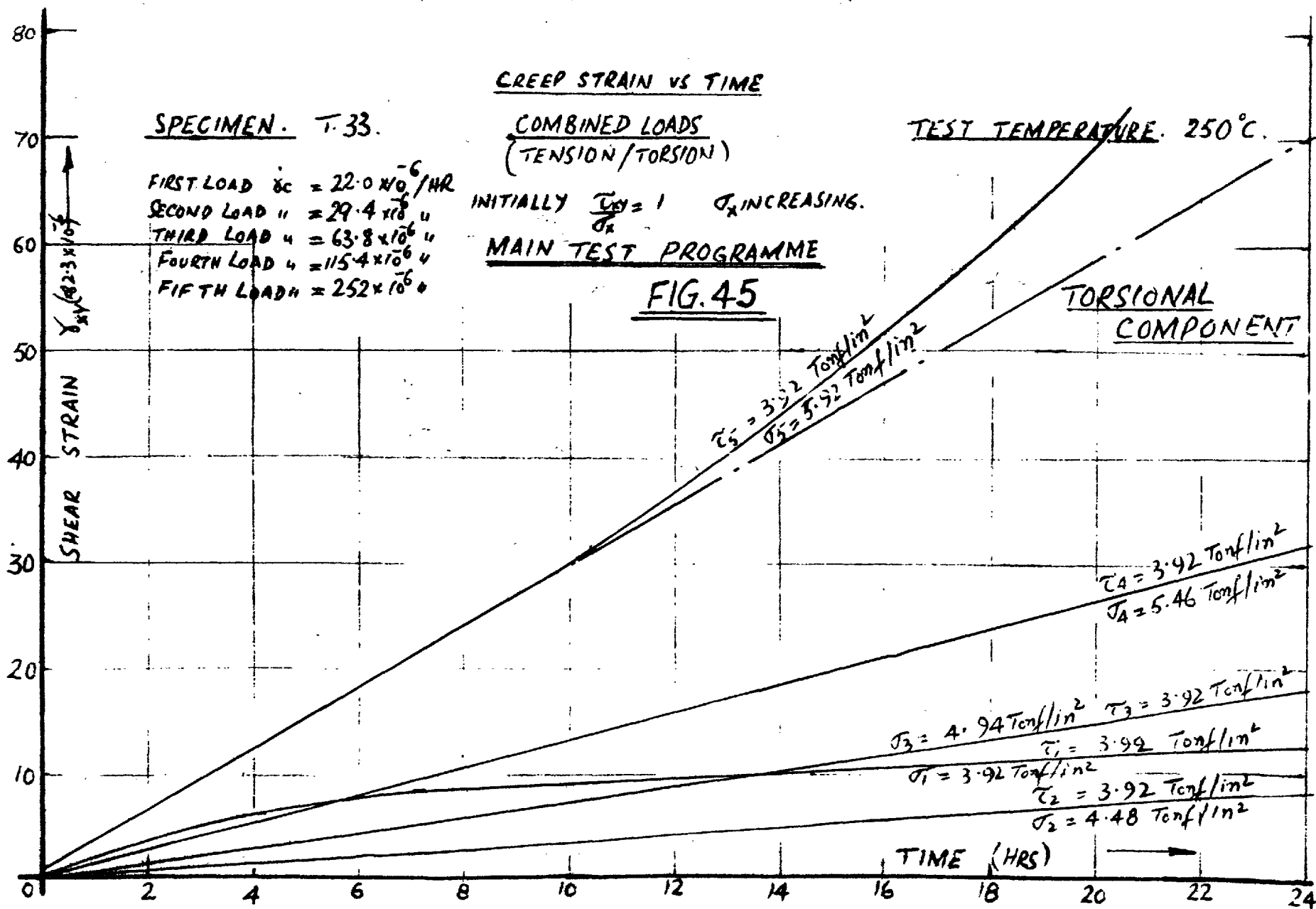
MAIN TEST PROGRAMME

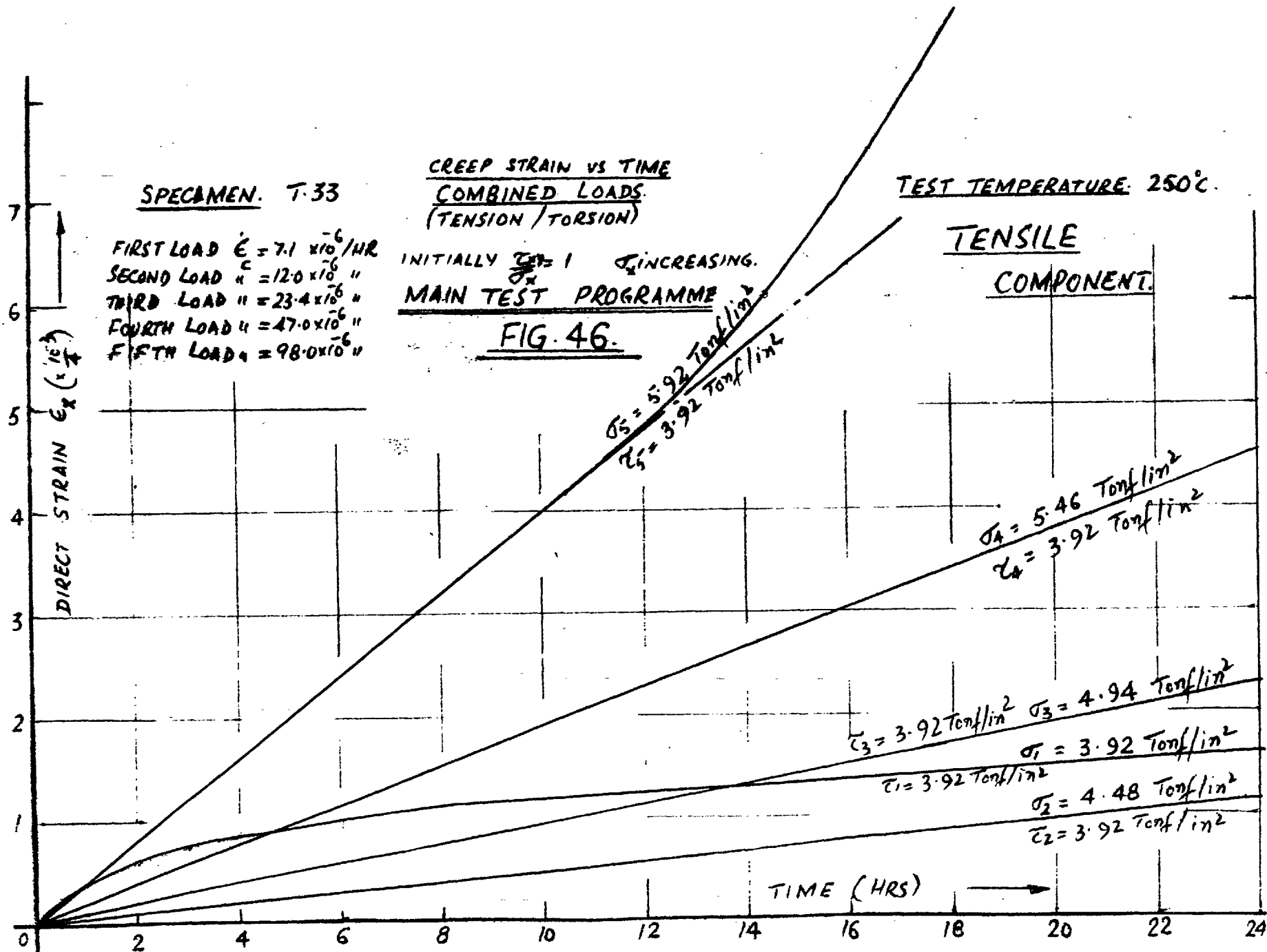
FIG. 42

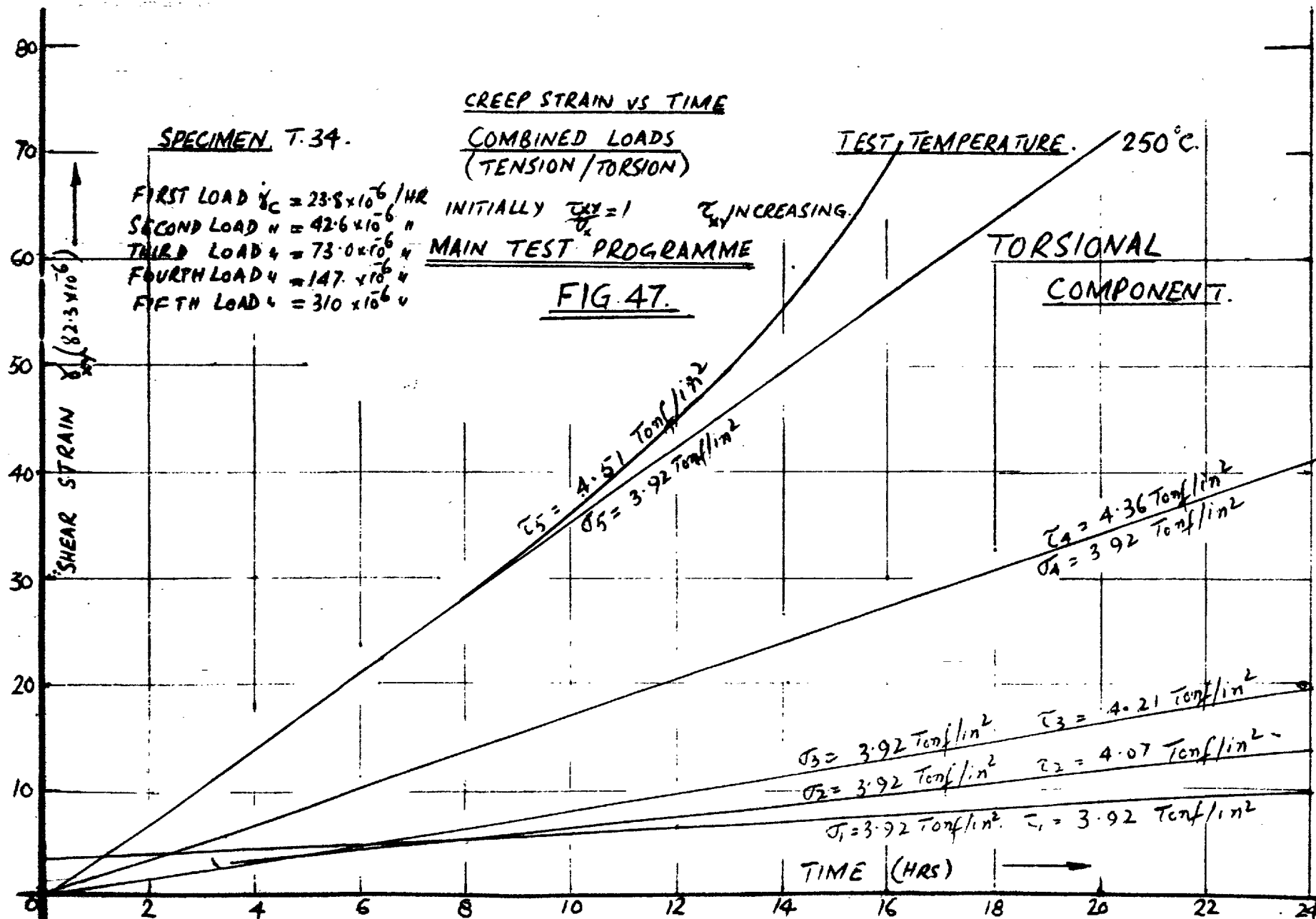


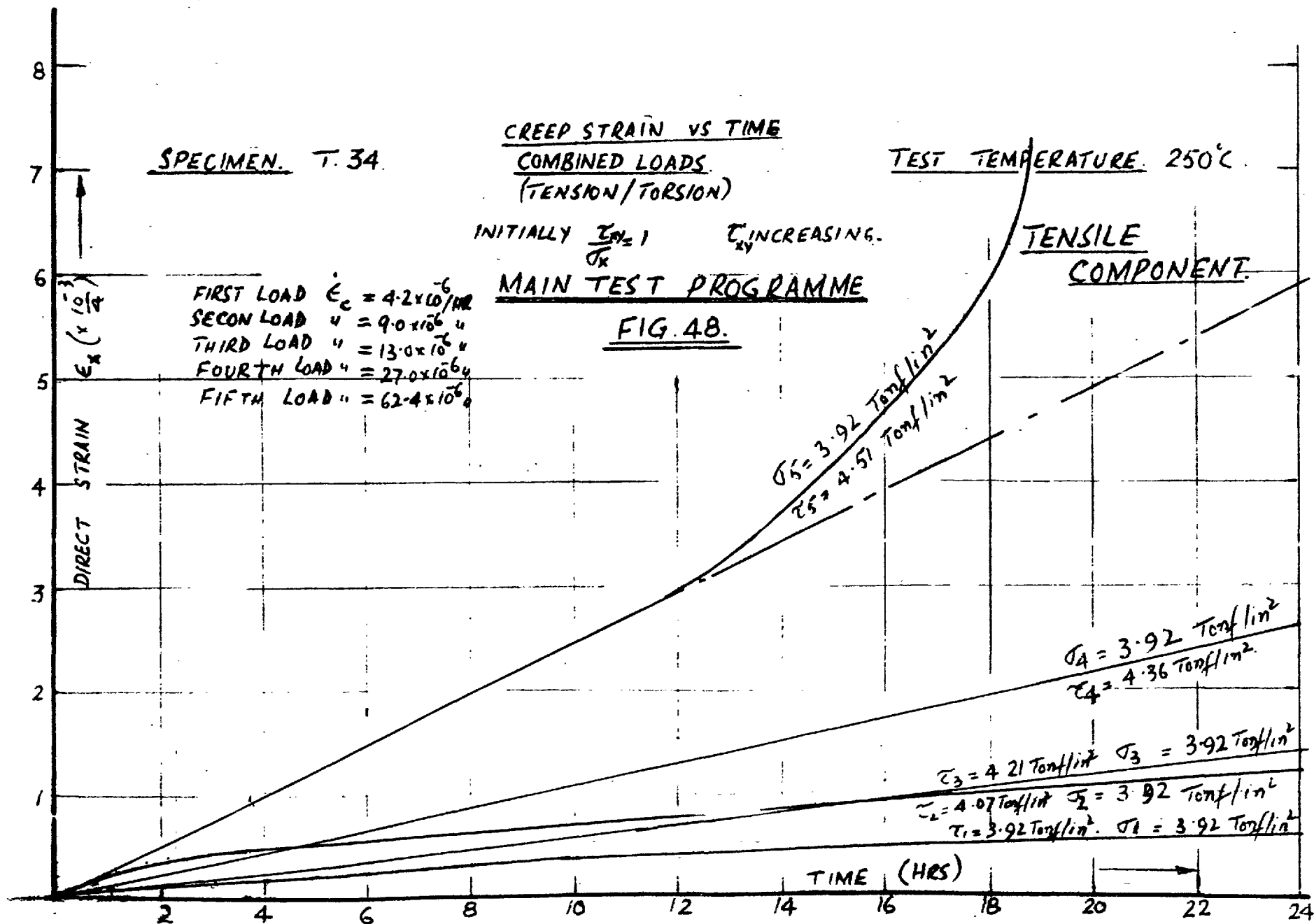




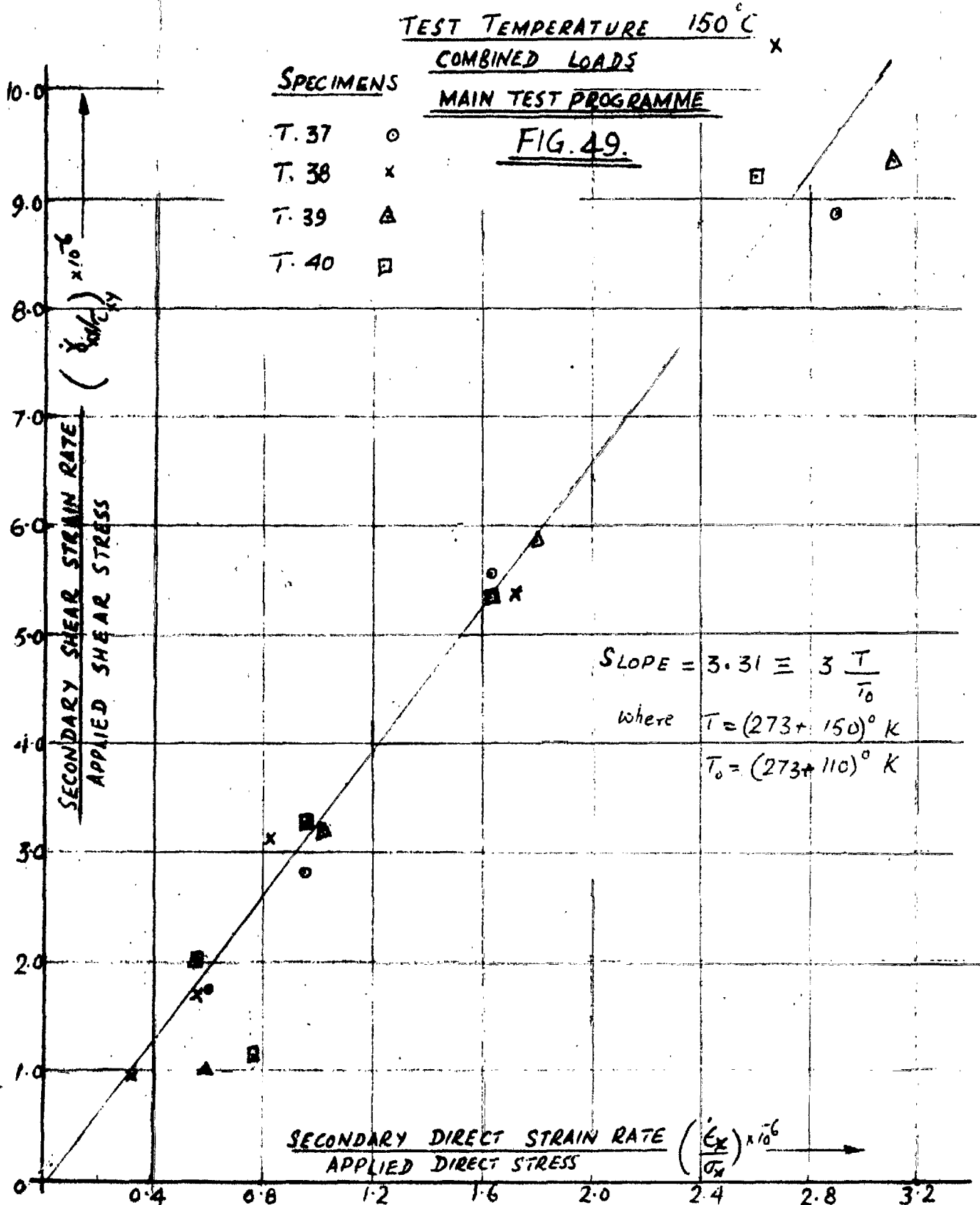






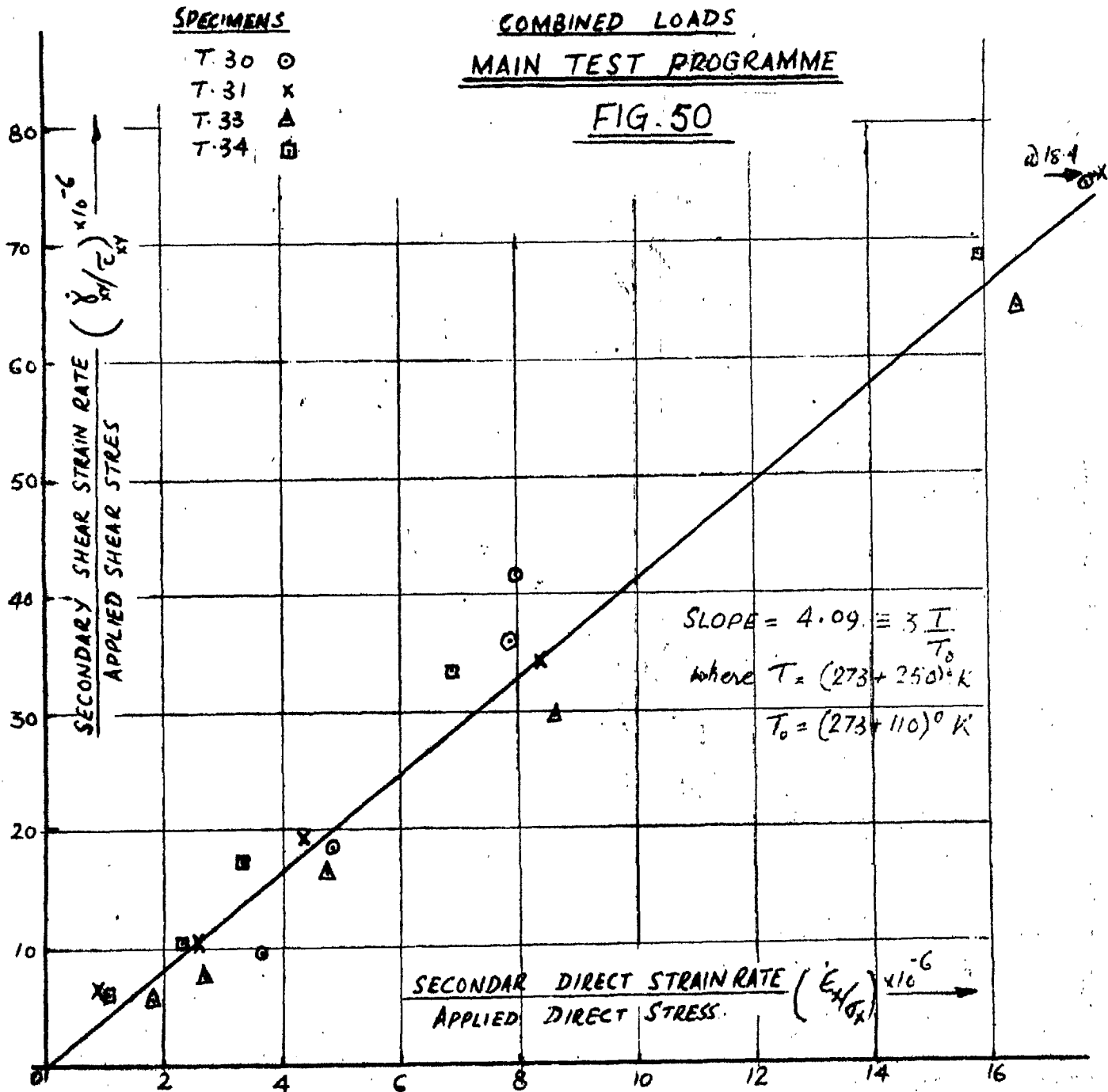


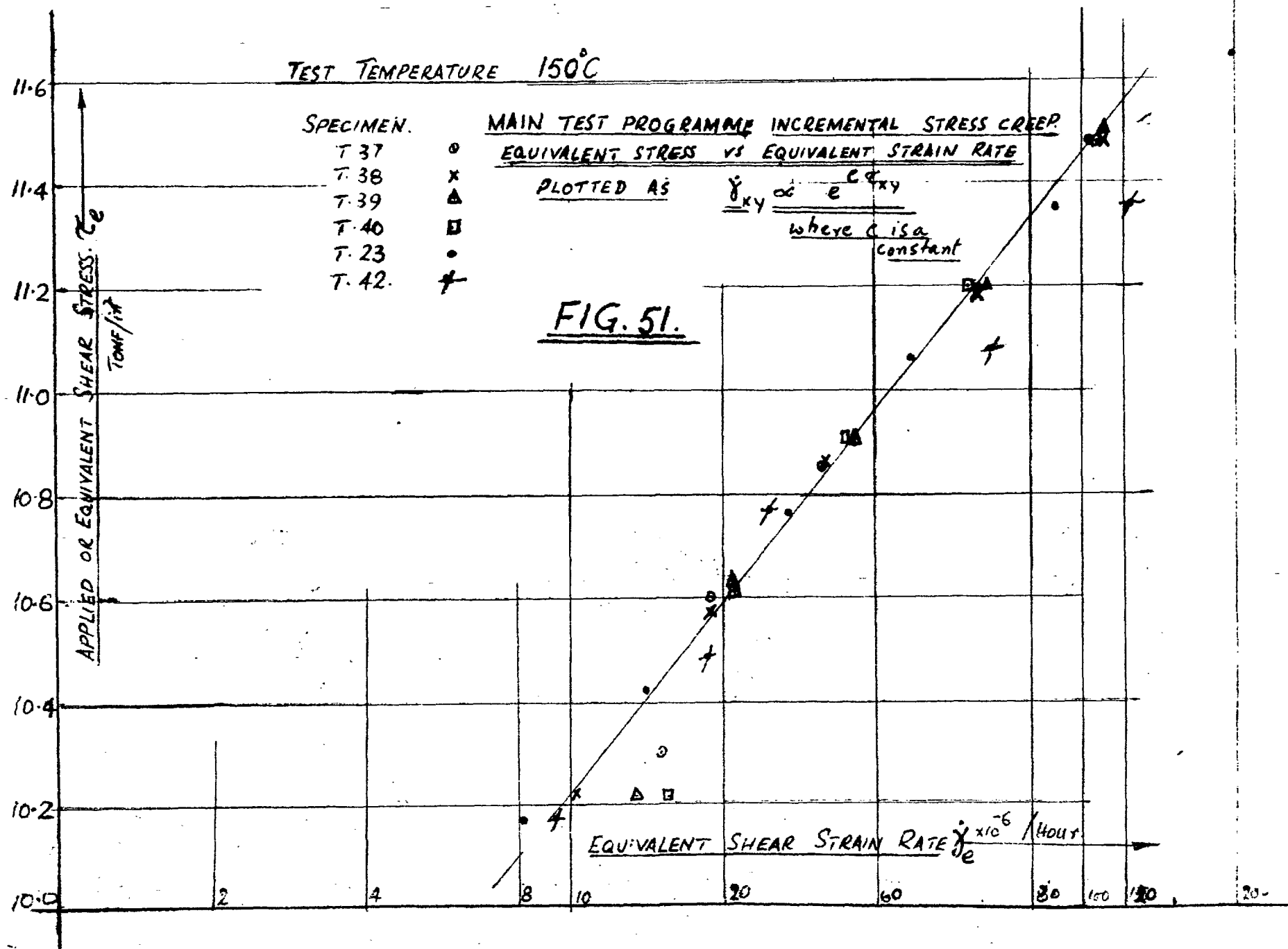
EXPERIMENTAL AGREEMENT WITH THE STRESS EQUIVALENCE & THE STRAIN RATE EQUIVALENCE EQUATIONS AS DERIVED FROM THE 'WORK PRINCIPLE' FOR COMBINED TENSION-TORSION INCREMENTAL STRESS STEADY STATE CREEP AT 150°C.



EXPERIMENTAL AGREEMENT WITH THE STRESS EQUIVALENCE &
THE STRAIN RATE EQUIVALENCE EQUATIONS AS DERIVED FROM THE
'WORK PRINCIPLE' FOR COMBINED TENSION-TORSION INCREMENTAL
STRESS STEADY STATE CREEP AT 250°C.

TEST TEMPERATURE 250°C





TEST TEMPERATURE 250°C.

MAIN TEST PROGRAMME INCREMENTAL STRESS CREEP.

EQUIVALENT STRESS VS EQUIVALENT STRAIN RATE

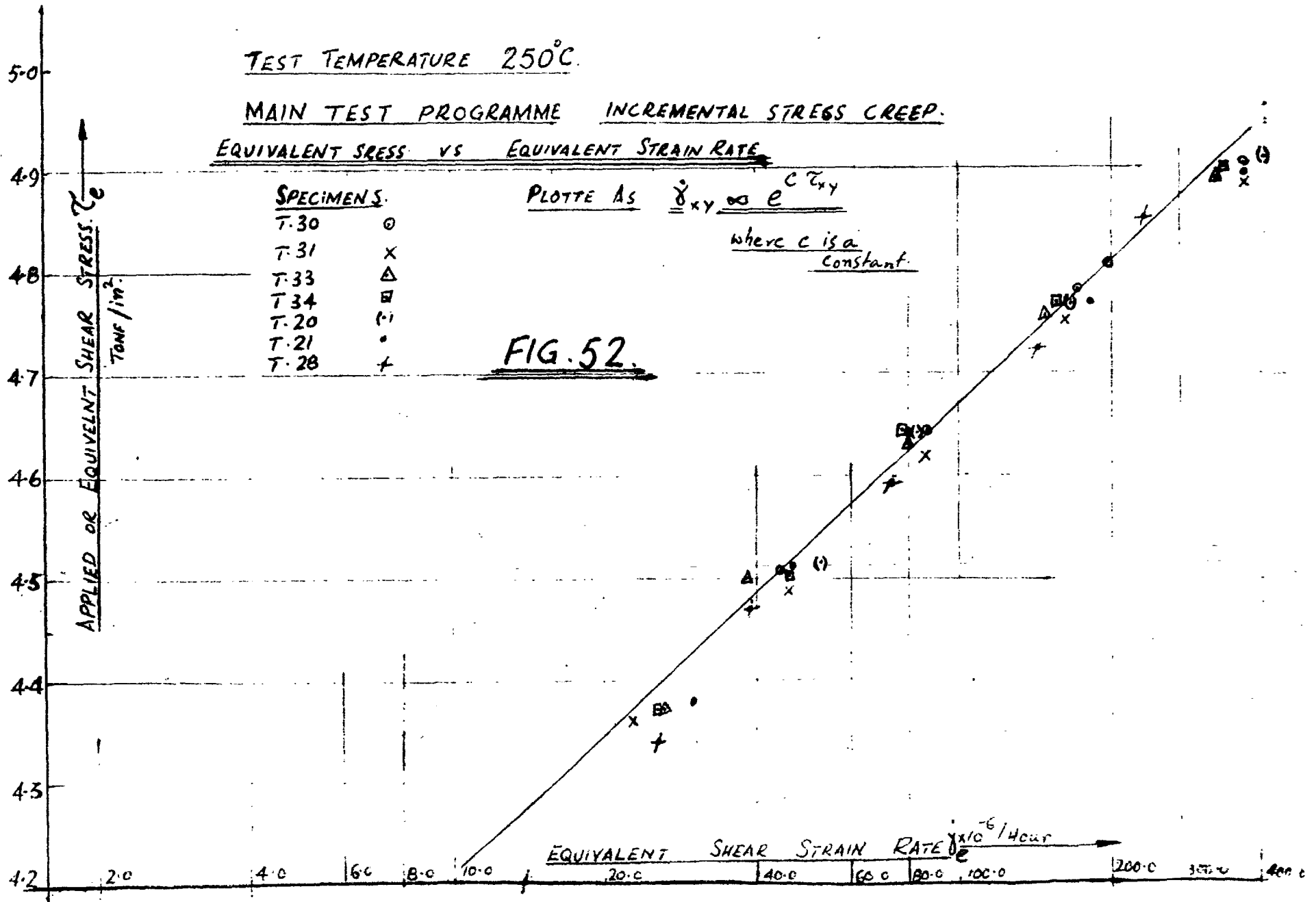
SPECIMENS.

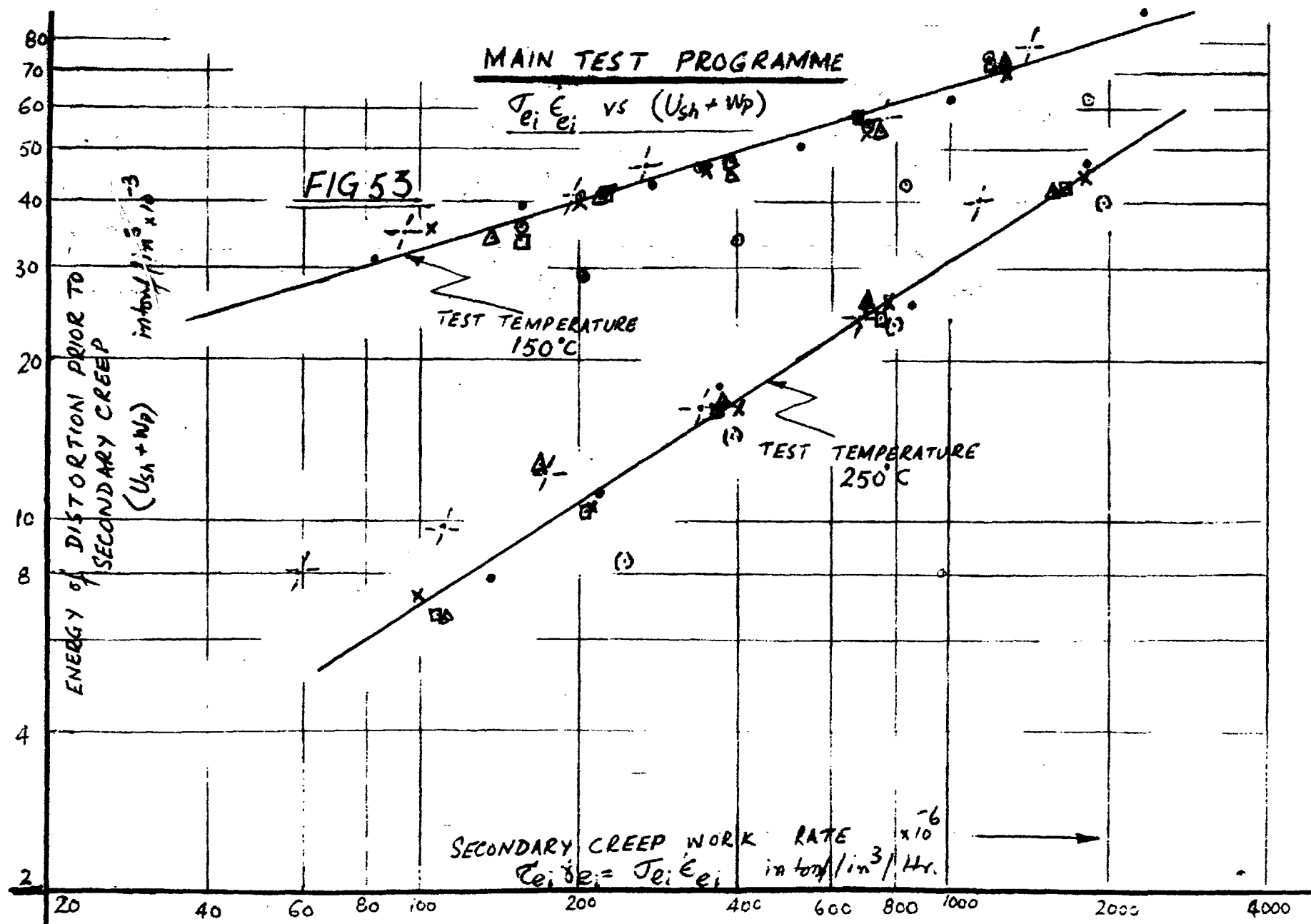
T.30	○
T.31	x
T.33	△
T.34	□
T.20	(.)
T.21	•
T.28	+

PLOTTE AS $\dot{\gamma}_{xy} \propto e^{c\tau_{xy}}$

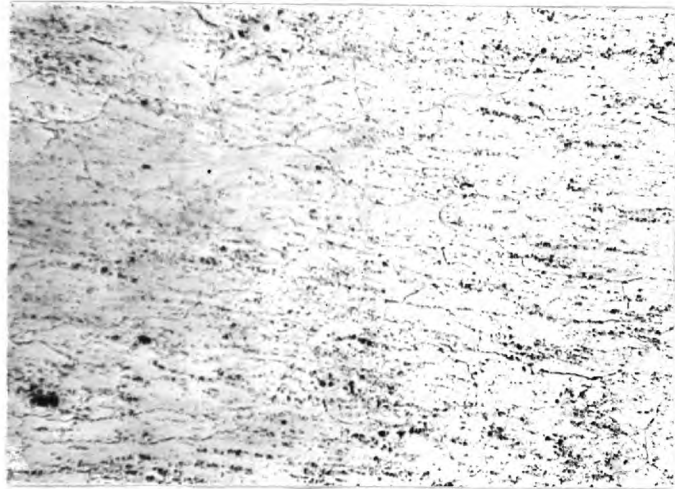
where c is a
constant.

FIG. 52.



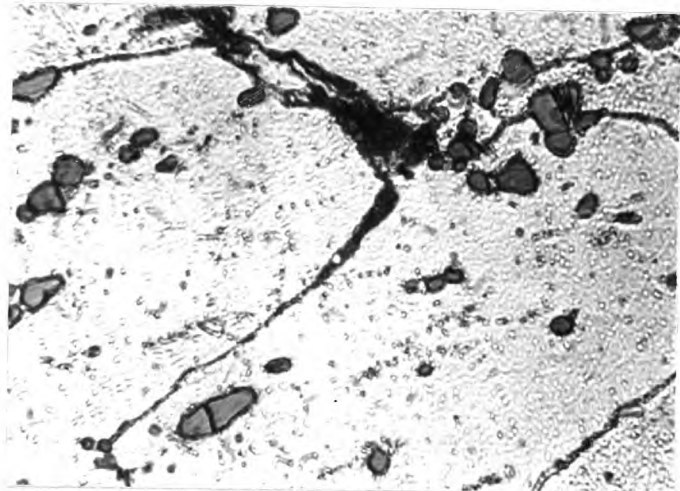


- (i) Condition - as supplied
Magnification x 55



- (ii) Static tension test to destruction. Near section of fracture.
Magnification x 905

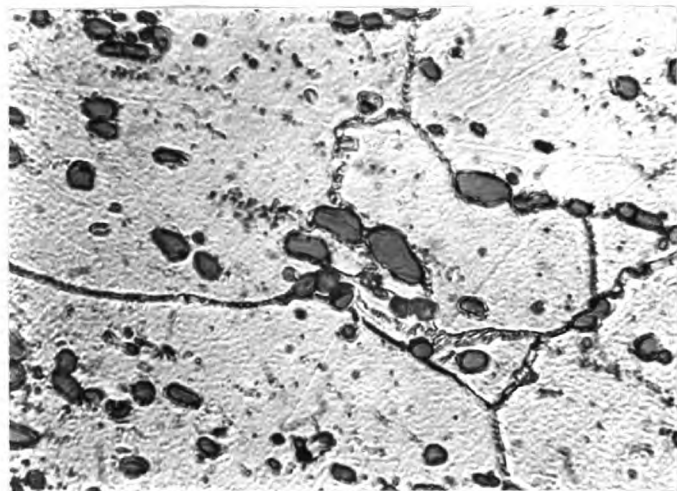
Heavy slip and cracks visible as dark grain boundaries.



- (iii) Static tension test to destruction.

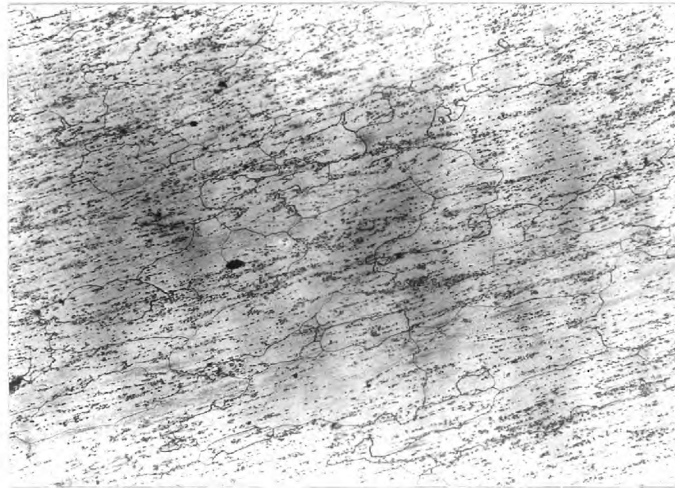
$\frac{1}{4}$ in. away from the section of fracture.

The traces of heavy slip and cracks visible along grain boundaries.



one inch gauge length specimen ① - Room Temperature 20°C

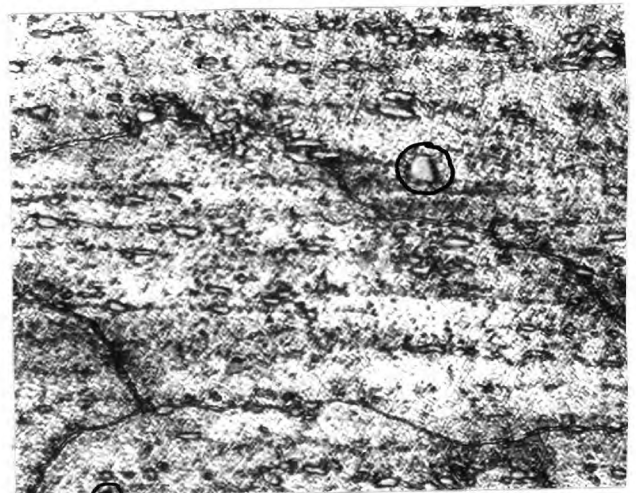
FIG. 54 (See page 60)



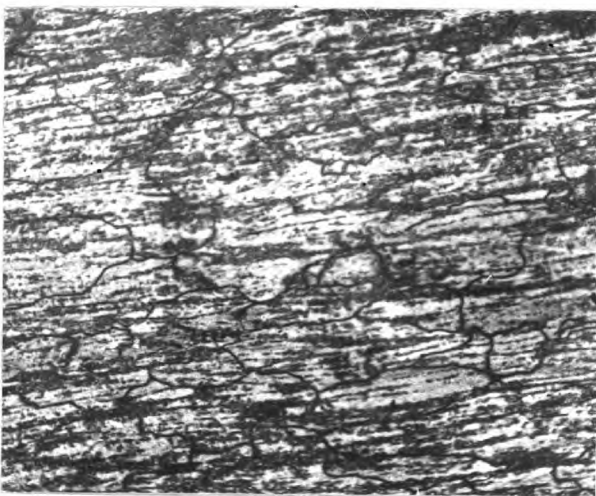
(i) Unstrained Soaked for 140 hours @ 150 deg. C
Magnification x 55



(ii) Tensile Creep @ 150 deg. C
soaked for 400 hours
Magnification x 55



(iii) Tensile Creep @ 150 deg. C. as in (ii)
soaked for 400 hours
Magnification x 905



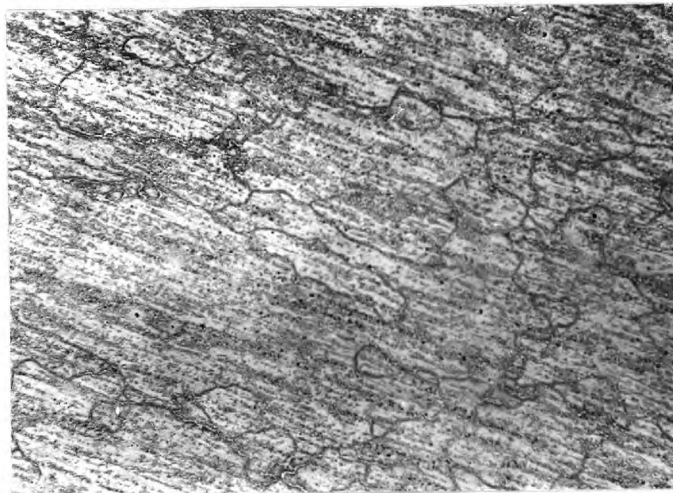
(iv) Tensile Creep @ 200 deg. C.
Tubular Specimen soaked for 100 hours
wall thickness.
Magnification x 55



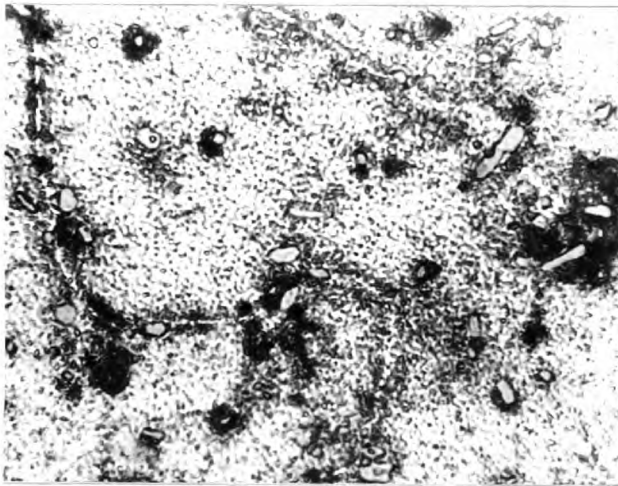
(v) Tensile Creep @ 200 deg. C.
Tubular Specimen soaked for 100 hours
Wall surface.
Magnification x 55

Precipitates and cavities are encircled for reference. In the higher magnification plates the precipitates are light coloured particles and the dark regions are cavities.

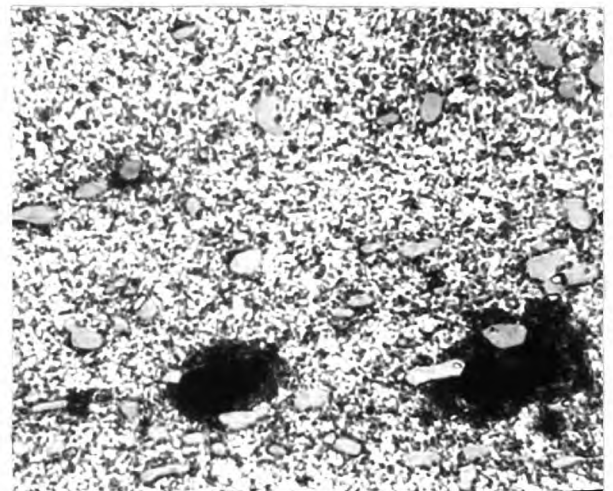
Compare grain size
with that of Fig. 54(i)



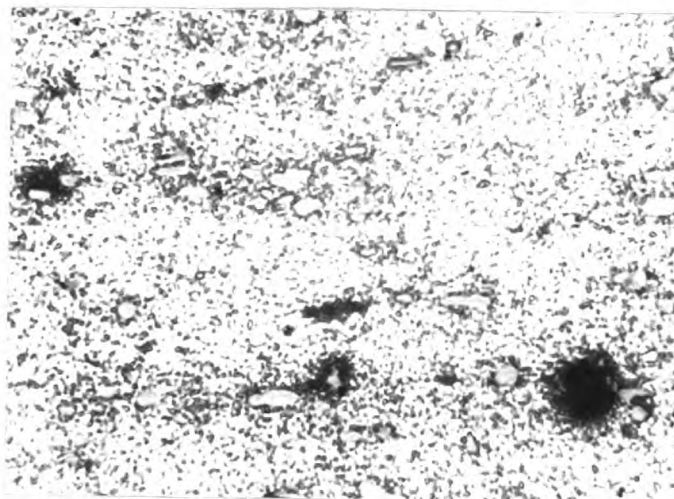
(i) Unstrained - soaked for 140 hours
Magnification $\times 55$



(ii) Tensile creep at 250°C soaked for 70 hours
Transverse cross-section
Magnification $\times 905$



(iii) Tensile creep at 250°C soaked for 70 hours
Axial cross-section near neck formation
Magnification $\times 905$



(iv) Tensile creep at 250°C soaked for 70 hours
Axial cross-section $\frac{1}{2}$ in. away from neck formation
Magnification $\times 905$

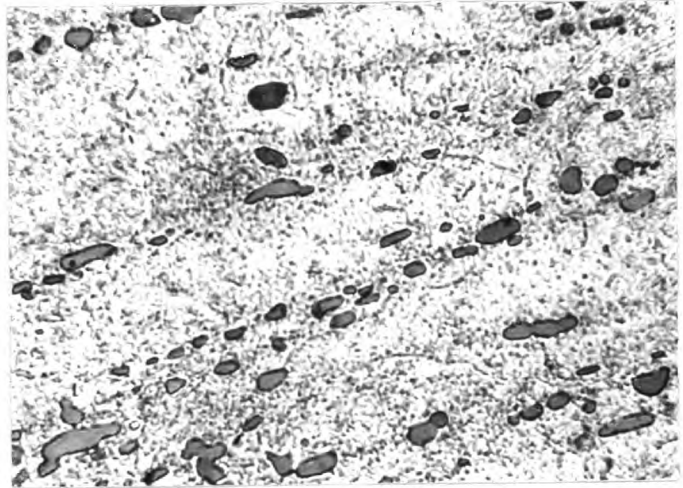
Precipitates are light coloured particles while cavities
are the dark regions.

FIG. 56(a) (See pages 60, 61)

- (i) Static torsion test to rupture
at 250°C.
6 hour soak.
Wall surface

Magnification $\times 905$

Precipitate is aggregated
along grain boundary in the
axial direction.

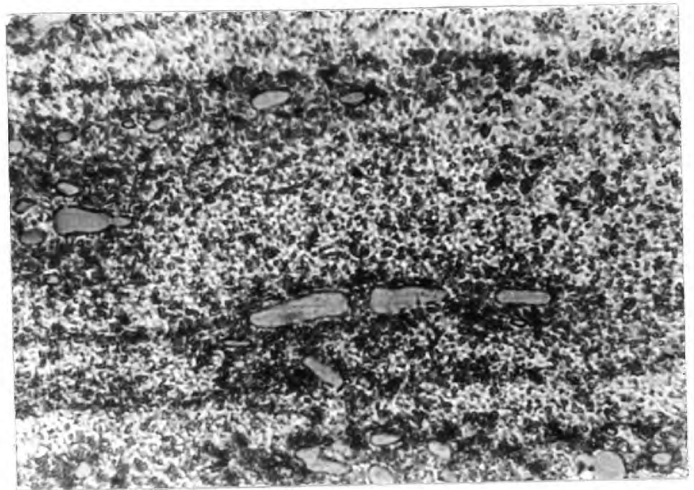


- (ii) Torsional creep at 250°C
Total effective soak period
106 hours.

Magnification $\times 905$

Wall thickness

Traces of heavy slip visible
along the grain boundary.
Heavy precipitation
throughout.



- (iii) Torsional creep at 250°C.
Same as (ii).

Magnification $\times 905$.

Wall surface.

Crack propagation visible
along the aggregated grain
boundary.

Dark region shows the
development of cavities.

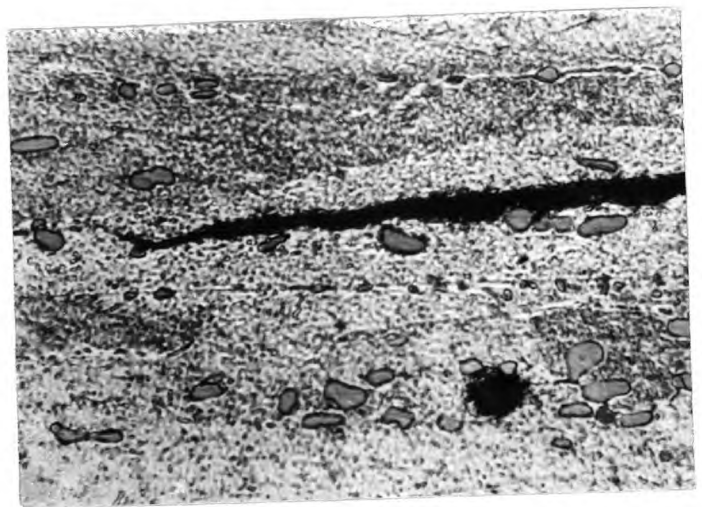


FIG. 56(b) (See page 61)

APPENDIX 3

- (i) Theoretical calculations - current stress
- (ii) Complex Creep Machine (published work)

(i) Theoretical Calculations - current stress

One inch gauge length specimens:-

For a series of tensile creep test specimens on unloading, the permanent tensile strain and the permanent reduction in diameter were measured. It was found that the axial strain was ϵ_x and the diametral strain was $\epsilon_x^{\frac{1}{2}}$. This observation is consistent with the classical law of plasticity that the change in volume is zero. Therefore the corrected stress values were determined by the relation:-

$$\text{Actual tensile stress} = \text{Nominal stress} (1 + \epsilon_x).$$

In the 10 : 1 arm ratio Denison creep tester, an applied load of 1 lbf is equivalent to a tensile stress of 0.1616 tonf/in^2 for the test specimens of 0.1875 in. diameter.

Tubular specimens:

In the tests using tubular specimen the maximum permanent strains do not exceed 1.00 per cent and no stress correction was necessary.

For equivalent stresses and strain rates the equations (37) and (48) were employed.

$$\text{Direct stress component } \sigma_x = \frac{\text{Axial load}}{\text{Cross sectional area}}$$

$$\text{Mean Shear stress component } \tau_{xy} = \frac{C}{2 R_o R_i t} - \frac{1}{B \sqrt{T/T_o}} \log_e \left(\frac{R_o}{R_i} \right)^{1/2}$$

where C = Applied torque, R_i and R_o are inner and outer diameters of the specimen and t is the wall thickness; the remaining terms have been defined previously.

For tensile incremental stress creep it was assumed and experimentally confirmed that the secondary creep rate is defined by the law

$$\dot{\epsilon}_x = A e^{B\sigma_x} \quad - (i)$$

For pure torsion

$$\dot{\gamma}_{xy} = \sqrt{3 T/T_0} \dot{\epsilon}_x \text{ from equation (48)} \quad - (ii)$$

$$\text{and } \tau_{xy} = \frac{\sigma_x}{\sqrt{3 T/T_0}} \text{ from equation (37)} \quad - (iii)$$

Consider an annular element at a radius r in the section of the tube where the shear stress has a value τ and the shear strain rate is $\dot{\gamma}$

From equations (i), (ii) and (iii)

$$\frac{\dot{\gamma}}{\sqrt{3 T/T_0}} = A e^{B \sqrt{3 T/T_0} \tau} \quad - (iv)$$

and at the outer radius R_0

$$\frac{\dot{\gamma}_0}{\sqrt{3 T/T_0}} = A e^{B \sqrt{3 T/T_0} \tau_0} \quad - (v)$$

If plane sections remain plane and the original geometry is maintained, then

$$\frac{\dot{\gamma}}{\dot{\gamma}_0} = \frac{r}{R_0} \quad - (vi)$$

From equations (iv), (v) and (vi)

$$\tau = \tau_0 - \frac{1}{B \sqrt{3 T/T_0}} \log_e \left(\frac{R_0}{r} \right) \quad - (vii)$$

From equilibrium

$$\begin{aligned} C &= \int_{R_i}^{R_0} 2\pi r^2 \tau dr \\ &= 2\pi \int_{R_i}^{R_0} \left[\tau_0 r^2 dr - \frac{1}{B \sqrt{3 T/T_0}} r^2 \log_e \left(\frac{R_0}{r} \right) dr \right] \\ &= \frac{2}{3} \pi \left[(R_0^3 - R_i^3) \tau_0 - \frac{R_0^3}{B \sqrt{3 T/T_0}} \left(\frac{1}{3} - \frac{\log_e \frac{R_0}{R_i} + \frac{1}{3}}{(R_0/R_i)^3} \right) \right] \end{aligned}$$

t is small as compared to R_i i.e. $R_i = 10t$

$$R_o^3 - R_i^3 = 3 R_i R_o t \text{ and } \frac{1}{3} - \frac{\log_e \frac{R_o}{R_i} + \frac{1}{3}}{(R_o/R_i)^3} = + \frac{3}{2} \left(\frac{t}{R_i}\right)^2$$

$$C = 2\pi \left[R_o R_i t \tau_o - \frac{R_o^3}{2B\sqrt{3T/T_o}} \left(\frac{t}{R_i}\right)^2 \right]$$

$$C = 2\pi R_o R_i t \tau_o \left[1 - \left(\frac{R_o}{R_i}\right)^2 \left(\frac{t}{R_i}\right) \frac{1}{2B\sqrt{3T/T_o}} \frac{1}{\tau_o} \right]$$

Since B , T/T_o and τ_o are all greater than 1

$$\tau_o' = \frac{C}{2\pi R_i R_o t}$$

Substituting in equation (vii)

$$\tau = \frac{C}{2\pi R_i R_o t} - \frac{1}{B\sqrt{3T/T_o}} \log_e \left(\frac{R_o}{r}\right)$$

$$\text{The mean radius} = \sqrt{R_i R_o}$$

Hence

$$\text{the mean shear stress } \tau_{xy} = \frac{C}{2\pi R_o R_i t} - \frac{1}{B\sqrt{3T/T_o}} \log_e \left(\frac{R_o}{R_i}\right)^{1/2}$$

$$\frac{R_o}{R_i} = 1.1 \text{ for tubular specimens}$$

$$\tau_{xy} = \frac{C}{2\pi R_o R_i t} - \frac{1}{B\sqrt{T/T_o}} \frac{0.0488}{1.732}$$

The last term at 150°C equals 0.025 Tonf/in^2

and at 250°C equals 0.010 Tonf/in^2 .

This variation caused by the last term is less than $\frac{1}{4}$ per cent and therefore the shear stress values were determined using the formula

$$\tau_{xy} = \tau_o = \frac{C}{2\pi R_o R_i t}$$

Paper 29

A COMPLEX CREEP TESTING MACHINE

By S. B. Mathur^{*} and J. M. Alexander[†]

A machine has been developed which enables accurate loading of thin-walled tubular specimens in combined tension and torsion and at elevated temperature.

Loading is carried out directly through lever systems incorporating novel compensating mechanisms which obviate geometrical changes owing to distortion of the specimen. Axiality of loading is ensured by a double knife-edge universal joint system developed by Shelton.

The furnace and measuring systems are also described.

INTRODUCTION

DURING THE PAST CENTURY most of the investigations on the behaviour of materials have been in uni-axial or single load systems. Recently it has been recognized that the multi-axial loading induced in service conditions requires combined load tests. Three main combinations of loading which can be readily achieved with tubular specimens have been usually employed:

- (a) combined tension and torsion;
- (b) combined torsion and internal pressure;
- (c) combined tension and internal pressure.

In many of the arrangements employing tension and torsion there are provisions for incorporating internal pressure.

Most commercially produced test equipment employs strain loading, though a few use direct loading techniques. For combined loading a number of machines have been developed both in Britain and other countries by individual investigators, but there appears to be a need for the development of commercial equipment of this type. There also appears to be a lack of suitable commercially available auto-recording extensometers for such tests. In the experimental investigation of the plasticity and creep behaviour of metals a great deal of work has still to be done under combined loads. Hence the need for such test equipment is likely to continue.

The MS. of this paper was received at the Institution on 16th October 1964.

^{*} Senior Lecturer in Mechanical Engineering, Kingston College of Technology, Kingston, Surrey.

[†] Professor of Engineering Plasticity, Department of Mechanical Engineering, Imperial College, London.

A combined tension-torsion machine was designed on the basis of an original design prepared by Shelton (1).[‡] The machine is intended for combined stress creep tests at elevated temperatures. The nature of the work demands a high degree of accuracy, and every endeavour has been made to obviate errors. Loads are applied by dead-weights, the filling of buckets with water giving a continuous control of their magnitude. For applying tension a compound lever system is used, whilst torque is induced by a single lever. The deformation of the test specimen induces movement of these load levers and there is a possibility that redundant loads may be brought into play. This must be avoided and a travelling wedge mechanism is employed for the tensile load lever system and a worm-wheel and screwjack device for the torque applying lever to return them to their original configurations.

A combined extensometer for measuring linear and angular displacements was designed on the basis of one developed at the National Engineering Laboratory (2). The measurement of displacement is mechanical. Optical reflection or out-of-balance voltage measurement has been avoided. Such techniques require mechanical links to project from within the furnace and it is felt that this may induce significant errors owing to differential thermal expansion.

The torsion arrangement used precluded the use of a muffle furnace which could slide on vertical guides and hence a split furnace construction had to be adopted. The distribution of windings was based on geometrical and output similarity from existing comparable furnaces, particularly one designed by Webster (3).

[‡] References are given in Appendix 29.I.

COMBINED TENSION-TORSION MACHINE

It was originally thought that a similar machine to that designed by Shelton could be used. However, after detailed examination it was found that radical redesign was essential for this particular application. The three following factors were of paramount importance:

- (a) The inertia of components and their corrosion were to be kept to a minimum.
- (b) Accuracy of alignment, strength, and rigidity of construction were all essential.
- (c) Accuracy of loading and its being maintained throughout the duration of each individual test by keeping the load levers to their initial configuration was essential.

These considerations dictated a light alloy construction, although for some components the resulting unduly large size made the use of steel necessary.

The overall dimensions are as follows:

height 10 ft 6 in length 6 ft 6 in width 3 ft

Load capacity:

25 000 lbf in tension 5000 lbf in in torsion

For the specimens used the maximum stresses that can be induced are 160 000 lbf/in² in tension and 58 000 lbf/in² in torsion.

Main frame

The main base is composed of steel channel members, Fig. 29.1 (1). A smaller rolled steel joist construction (2) is superimposed at one end of the main base.

A mild steel base plate (3) is bolted to these two structures. A water reservoir of 30-gal capacity (4) rests at the other end of the channel base as shown in Fig. 29.1. The mild steel base plate is one of three similar platforms which were all bored in line together for true alignment on assembly. The top Duralumin plate (5) and the steel base plate house four flanged spigots (6). Four solid cylindrical Duralumin columns (6a) are located in the flanged spigots to afford greater rigidity and strength at the locating ends. They are 7 ft long and thereby allow specimens of different dimensions to be accommodated. The central Duralumin platform (7) is free to slide along the columns and forms a rigid anchorage for one end of the specimen assembly. The entire tensile load is borne by the platform (7) and

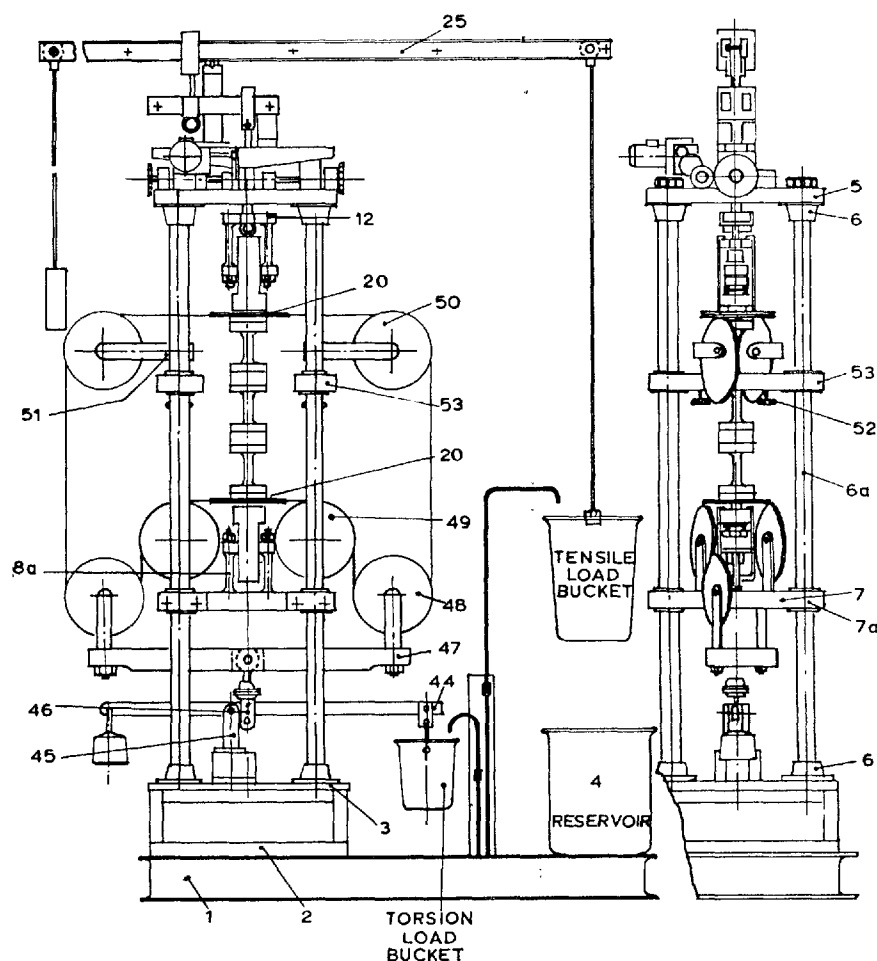


Fig. 29.1. Combined tension-torsion machine

hence it is essential that it be rigidly fixed to the columns when in position. For this purpose the sliding ends of the platform are machined to the shape of half bearings and similarly shaped big-end caps (7a) are also machined. For bearing surfaces, half-shell type bearings were machined from tubular Tufnol of appropriate size. Shims of thickness 0.006 in were used when boring the plate and caps together and it was observed that the resistance to sliding per column was 16 tonf. Tufnol was employed for these shell bearings to obtain the maximum resistance to sliding and to protect the columns against scouring if slip were to occur.

TEST SPECIMENS

In uni-axial tension any solid cross-section is satisfactory but under torsion the stress distribution for a solid section is not uniform along a radius. Also any plastic deformation would not be distinctly indicated whilst part of the cross-section is still within the elastic range of the test material. These factors indicate that a thin circular section must be employed so that a sensibly constant stress distribution exists across the section. The core diameter was chosen to be 1.000 in, with a wall thickness of 0.050 in. All critical dimensions are held to within a tolerance of ± 0.0002 in. A 4-in gauge length was chosen so that strain rates could be accurately determined from the data obtained.

Several different types of specimen ends were used in the preliminary combined load tests. It is essential that the location gives true alignment. Four axial strain gauges were affixed to a dummy specimen which was loaded in

tension. Over the entire load range, at no time was there a scatter in excess of $\pm \frac{1}{2}$ per cent of the recorded values. This was considered satisfactory.

Axial constraint is obtained by coarse fitting end threads. For torque transmission, square spigots are provided. The details of the test specimen are shown in Fig. 29.2.

THE SPECIMEN ANCHORAGE

As previously stated alignment is of prime importance. It is essential that anchoring and load transmitting inter-linkage should not induce any undesirable loads. Referring to Fig. 29.1 two steel studs (8a) are screwed into the platform (7). At the other end two Duralumin studs (8b) are screwed into a steel cross link (12). The other ends of the studs are bolted to a steel cross link (9) (see Fig. 29.3). This carries a hardened steel insert (10) to its full width together with end 'vee' plates and stop plates. This provides a rest for the knife edge of the shackles.

The two shackles each consist of two enclosing steel plates (11). Two Duralumin blocks (13 and 14) are bolted in a sandwiched fashion and thus form the housing for two knife edges orientated mutually at right angles. This forms a universal joint of high load capacity. The knife edges are made from light strength steel and are hardened and ground to 62–64 Rockwell C. They are rigidly set in a Duralumin block (15) as shown. Blocks (13) and (9) have suitable recesses for the knife-edge rest plates. The block (14) has a suitable recess for locating a double thrust bearing which has a thrust capacity well in excess of the load capacity of the machine in tension.

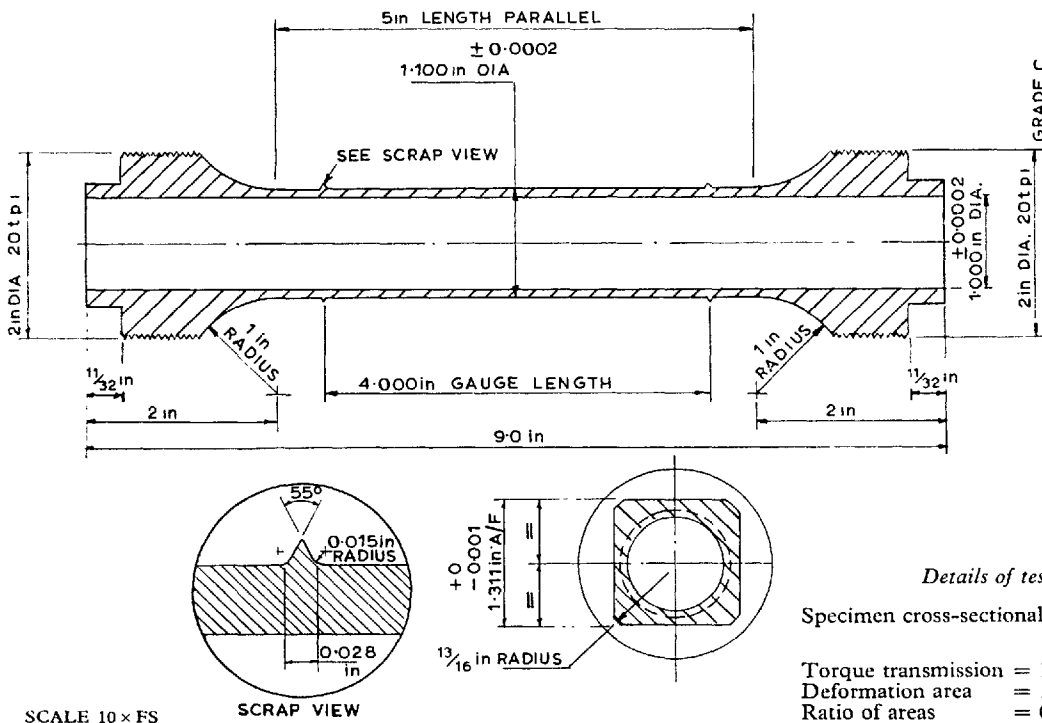


Fig. 29.2. Specimen and details

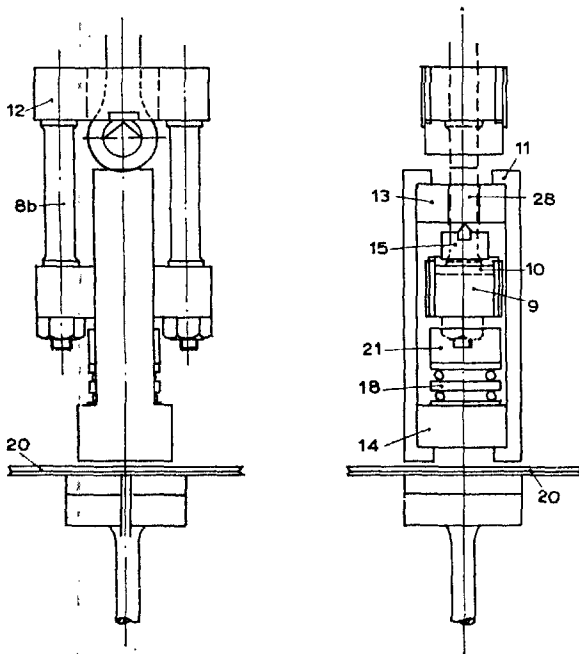


Fig. 29.3. Load transmitting shackles incorporating knife edges and thrust bearings

The two shackle assemblies thus provide two universal joints at each end of the test specimen assembly and simultaneously afford complete torsional freedom under load.

Referring to Fig. 29.4 the specimen location can be clearly seen. It consists of two torsion meter cells (16), the test specimen, and two torque-tension transmitting shafts (17). The transmission shaft is part of an assembly comprising shaft (17), the locknut (21) for thrust bearing (18), and the torque transmission block (19). The shaft is flanged with a square machined boss at its one extreme and is further turned along its length. The square boss is located in the transmission block (19) which in turn is dowelled and bolted to the torque pulley (20). The pulley is located on the shaft which passes through the block (14) and the thrust bearing (18) and is locked by the nut (21). The flanged end is counter-bored and an aligning dowel (22) lines up with the torque cell (16). The axial link is provided by the threaded flange (23) which screws on to the torque cell or the test specimen and is bolted to the transmission block in turn.

The flanged end is counter-bored and an aligning dowel (22) lines up with the torque cell (16). The axial link is provided by the threaded flange (23) which screws on to the torque cell or the test specimen and is bolted to the transmission block in turn.

The complete alignment can be seen in Figs 29.3 and 29.4.

THE LOADING SYSTEM

Tension

The tensile load is transmitted to the test specimen through a cross member (12). The load linkage is by a compound lever, using dead-weight and water which is pumped into the loading bucket to cover the range of load variation.

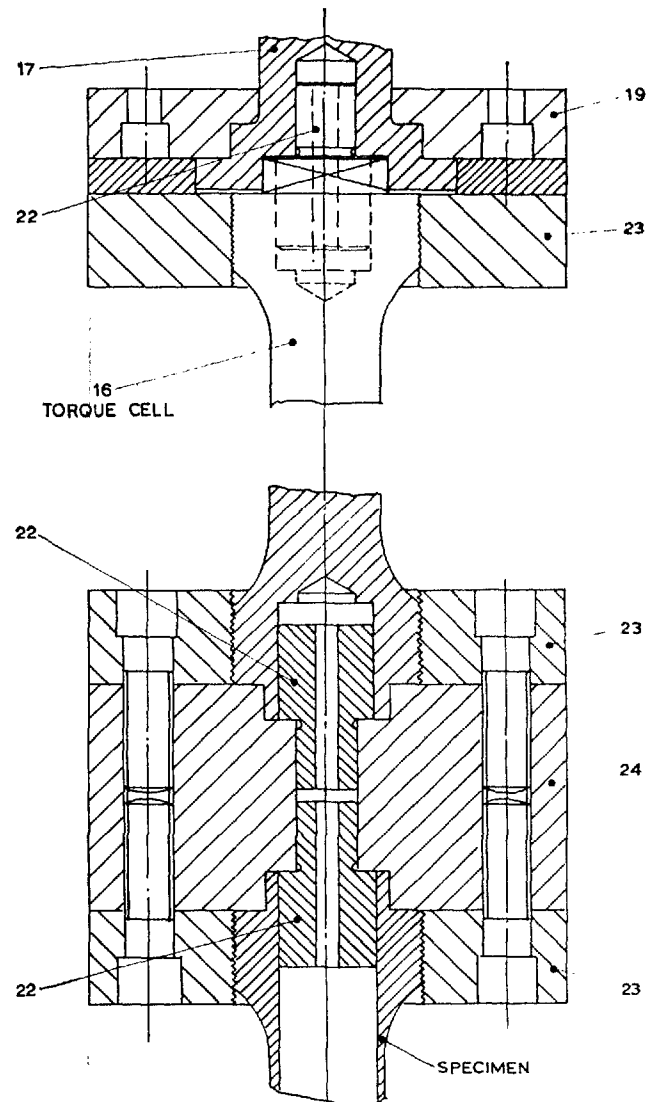


Fig. 29.4. Specimen anchorage assembly

With reference to Figs 29.1 and 29.5 the primary lever (25) carries the loading bucket at one end and a balance weight, to compensate for the dead-weight of the leverage and all the interlinkage up to the specimen, at the other end. The primary magnification is 24. This magnified load acts at a connecting rod type of link (26) and through the knife edges is transmitted to the secondary lever (27). A further magnification occurs at this leverage and the overall magnification available at the secondary connecting rod link (28) is 96. The load in turn is thus transmitted to the shackles and hence on to the test specimen.

All knife edges have a 90-deg apex angle and rest on hardened and ground steel pads with end 'vee' plates of 120 deg included angle, allowing ± 15 deg rotation of knife edges. From Fig. 29.5, it can be seen that the point of application of load, the knife edge and the fulcrum are not in line both for the primary and secondary levers. Any

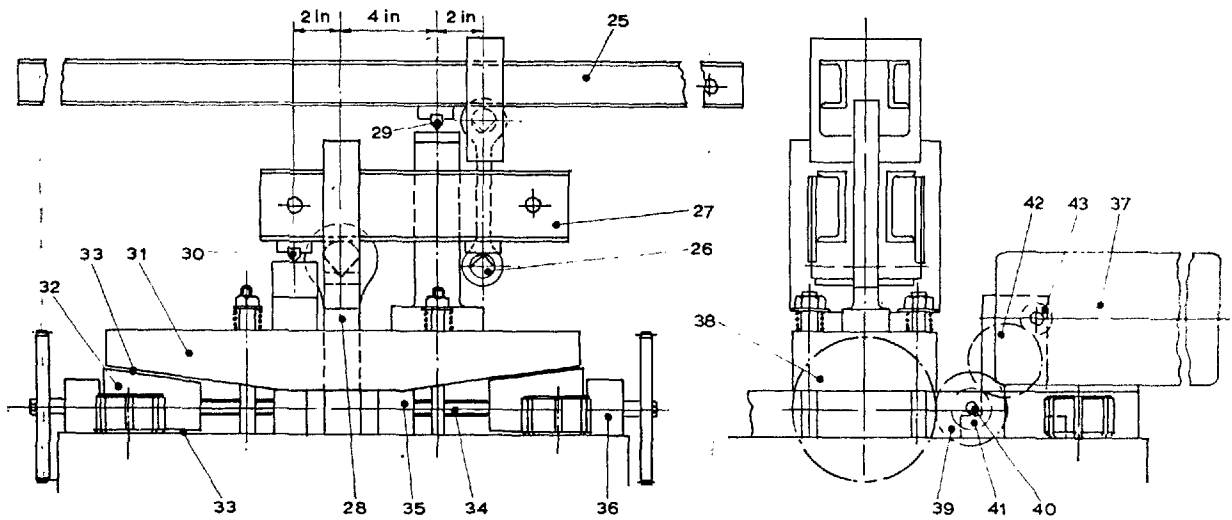


Fig. 29.5. Tension loading system

movement of the levers under load causes rotation of the levers and there is a strong likelihood of induced redundant loading. The rotation is caused by two effects, (a) extension of the specimen and (b) general extension throughout the interlinkage. It becomes essential that if the true applied loads are to be maintained constant throughout the test the geometric configuration of the entire loading linkages must remain unchanged. In most machines this is achieved by a travelling straining head attached to the lower end of the specimen. For this testing machine a novel device was designed which does not interfere with the geometry of the interlinkage and at the same time gives a negligible amount of dynamic effect.

If both the fulcrum supports (29) and (30), Fig. 29.5, are raised to the same extent as the overall extension of the specimen and the interlinkage, the geometry of the loading levers is maintained. This is achieved by rigidly fixing these fulcrum supports to a cross beam (31). The cross beam is tapered on the lower faces by an angle of 7 deg to the horizontal and rests on two hardened steel wedges having similar tapers on their top faces. The wedges (32) are ground and rest on a hardened steel insert in the top plate ((5), Fig. 29.1) of the main frame. The rubbing face of the cross beam is also similarly treated. To reduce frictional resistance in the wedges, flat needle-cage bearings (33) are sandwiched between the wedge faces. To actuate the wedges, they are counter-bored axially and carry interference fit bronze nuts which act as collars in screwjack arrangements. The driven screws are the shafts (34). They are housed in plummer block bars (35) and (36) which carry thrust bearings of suitable capacity to take the end loads as the wedges are actuated.

For creep tests the recordings of extension are spread over a long period and the strain rates are sometimes extremely low; hence for the wedges to be power driven a very slow driving speed is required. For this purpose a single-phase $\frac{1}{8}$ hp a.c. (reversing) geared motor with an

output shaft speed of 0.50 rev/min is used. The guide screw for the wedges has 8 t.p.i. The drive from the motor (37) to the shafts (34) is through a compound gear train with a step-down ratio from motor to shaft of 2.8:1. A lay shaft (40) had to be introduced and the gears are shown as (38), (39), (41), (42), and (43). (The layshaft rests in self-aligning ball-bearings.)

A motor speed of 0.50 rev/min gives a lift rate for the cross beam of 2.65×10^{-3} in/min. To obtain an idea of the obliquity of the primary lever on deflection, consider an overall stretch of $\frac{1}{20}$ in in the specimen linkage. This gives a load bucket vertical travel of 4.8 in or a rotation of 6 deg, so that for $\frac{1}{10}$ in extension the rotation is fast approaching the limiting value of 15 deg. It is observed from readings taken that the specimen extension varies from about 10 to 25 per cent of the overall displacement. The wedge travel gives a maximum lift of the cross beam of $\frac{3}{4}$ in. It may be pointed out, however, that within the capacity of the electric motor output, the taper of the wedges and associated parts can be easily modified to around 25 deg, so that a maximum vertical travel of 3 in can be obtained for those conditions where extensions are greater.

Automatic control of the levers to their original position is effected by a series of micro-switch controls. Two micro-switches placed $\frac{1}{8}$ in on either side of the primary lever energize or cut out the motor to suit traverse compensation either way. When the wedges are moved out for the lowest position an over-ride micro-switch de-energizes the motor and a similar switch controls the maximum inward travel. With the main supply switched on, control of the leverage to its original configuration is therefore entirely automatic. The sensitivity of control based on the $\frac{1}{8}$ in position of the micro-switches is therefore for every 0.005 in extension in the linkage or every 0.001 to 0.002 in of specimen extension.

On the current arrangement the torque required from

the motor at the maximum load is 5 lbf ft which is well within its maximum capacity of 25 lbf ft.

Tensile load measurement

To record the load most accurately, it is necessary to measure it as close to the specimen as possible. The dead-weight effect of the interlinkage has been avoided as far as possible and the magnification ratio is not introduced. The Duralumin studs (8b) are used as load cells. The $1\frac{1}{4}$ in shank diameter is machined to a $1\text{ in} \times \frac{3}{4}$ in rectangular section over a length of $3\frac{1}{2}$ in. The total cross-sectional area of $1\frac{1}{2}$ in² at a limiting stress of 7 tonf/in² gives a total load-measuring capacity of 10.5 tonf. If loads in excess of this value are required these studs would have to be replaced either with larger Duralumin studs or studs made from steel.

The strains induced in these cells are determined from four $\frac{1}{2}$ -in Saunders-Roe electrical strain gauges, connected in the usual closed Wheatstone bridge circuit. Two gauges are mounted axially while the other two are attached transversely. Each pair is mounted on the opposite faces of the rectangular section, thus compensating for any bending effects. There are no errors caused by temperature fluctuation or dynamic effects. The circuit is energized by an external 2-volt d.c. cell and the out-of-balance voltage is measured by a d.c. potentiometer capable of an accuracy of ± 2 microvolts.

The load cells were initially calibrated in a 10-ton dead-weight 'Buckton' machine up to 12 000 lbf. A slight curvature was noticed in the initial run. The cells were subjected to load cycling and finally a linear load characteristic was obtained. They were recalibrated 8 weeks later and a good order of repeatability was observed. There was no drift in the results obtained (see Fig. 29.6). The calibration of the load cells when assembled in the tension-torsion machine described is also shown in Fig. 29.6. The scatter and displacement of the two corresponding characteristics as seen is purely attributable to zero shift. In the tension-torsion machine at zero load the cells are under load owing to interlinkage dead-weight up to the test specimen. There is no deviation between the slopes of the two graphs and variation was calculated to be well under 0.1 per cent. This is an error which is within the range of accuracy of measurement and hence is treated as insignificant.

Torsion

Figs 29.1 and 29.7 show the torque loading device. Equal and opposite torques are applied to the ends of the specimen through the pulleys (20). The torque is applied by bringing into play tangential forces on the peripheries of these pulleys through a system of levers and other pulleys.

A simple lever load system is used. The lever (44) pivots about a needle bearing and spindle which is housed in a movable fulcrum support (45). This support is restrained in all directions but is free to travel vertically in a suitable guide. The lever carries a loading bucket at one end and

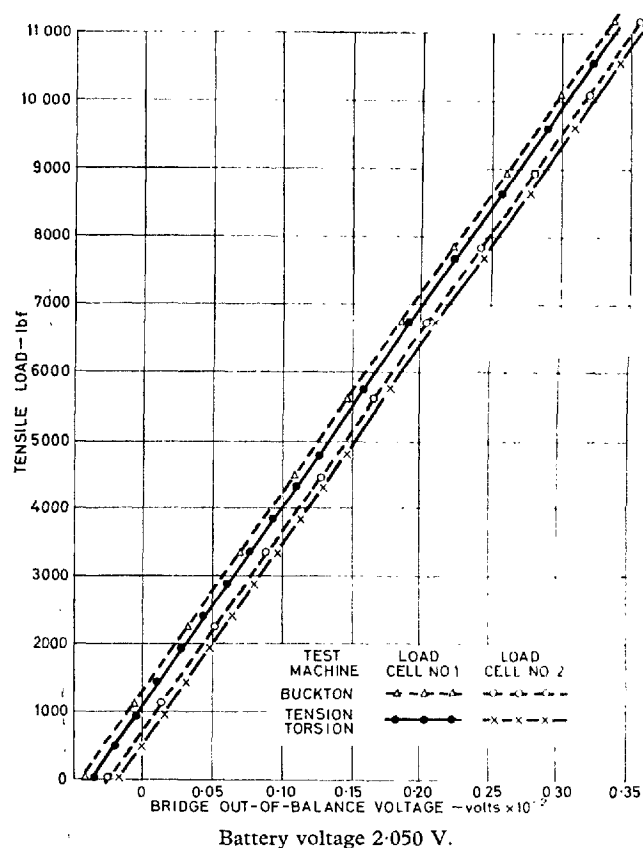


Fig. 29.6. Tension cells: comparison between load cell calibration characteristics

a counter-balance for the lever at the other end. A dead-weight equivalent of the initial load is applied at the bucket end and the range of load variation is obtained by pumping water into the bucket to suit. The load transmission point on this lever is a knife edge similar in construction to those described previously. It gives a magnification of $11\frac{1}{2}$ times through a universal joint assembly (46) to a cross beam (47). The two halves of the assembly are linked through a thrust bearing so that they can rotate relatively about a vertical axis. The upper half carries a ball-bearing and allows the assembly to move in a vertical plane, thus providing complete flexibility to the cross beam (47).

The cross beam carries two upright supports at the ends which are again free to rotate about vertical axes. These uprights carry 10 in diameter pulleys (48)—ball-bearing mounted—at their top ends which have 90 deg machined grooves on their peripheries to locate the load cable. Two similar uprights are suitably positioned on the platform (7) and are rigidly fixed in position on assembly. These carry pulleys (49). Another two pulleys (50) are mounted on forked arms (51) which hinge on the spindles of adjusting screws (52). These are located in the cross bars (53), locked on to the main columns of the machine as shown. Together with the two torque pulleys (20), the total of eight pulleys make up the system for

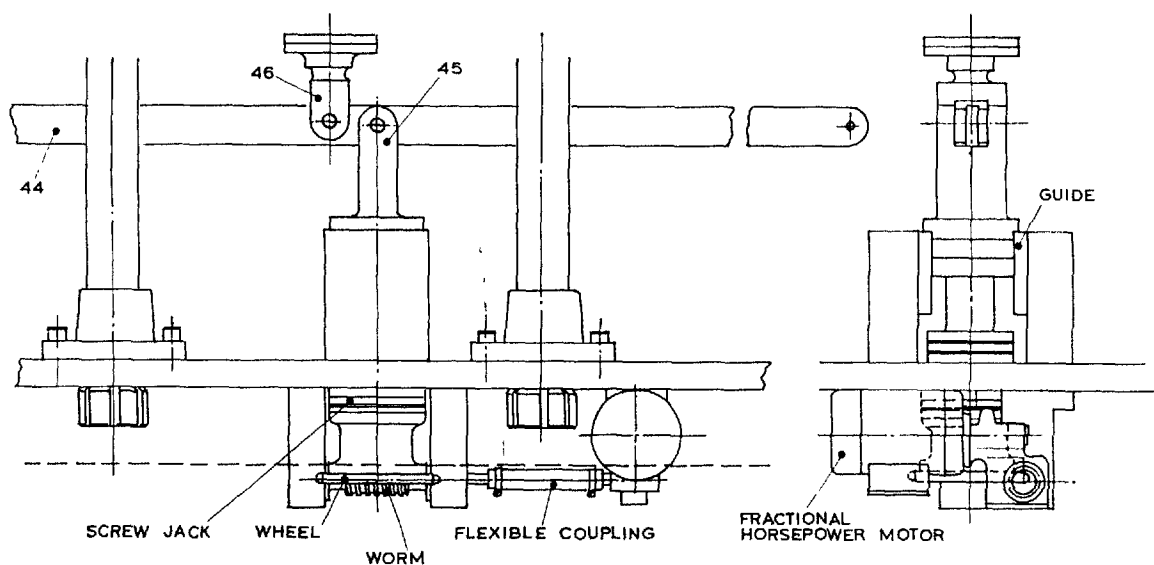


Fig. 29.7. Torque loading system

transforming the load on the cross beam (47) into torque on the test specimen. A $\frac{3}{16}$ in diameter stranded steel cable is looped over the pulleys and is rigidly anchored in counter direction to the pulleys (20). This induces opposite torques on the two ends of the specimen. The extension of the specimen and the interlinkage between the pulleys (20) takes up an additional length of the cable and alters the initial configuration of the loading levers. This induces redundancy and a false large load magnification results. Once again it is essential to re-establish the initial geometry. It is achieved by vertically traversing the support fulcrum (45) in its guide. The fulcrum support is bolted to a screwjack arrangement which is driven by a worm-wheel assembly and is energized by a fractional horsepower geared a.c. reversing motor (see Fig. 29.7).

For automatic operation and correction of the lever position, three micro-switches control the sequence of operations. One of the switches is an over-ride, limit stop switch while the other two switches control the initial position of the lever. The arrangement forms a complete control and is found to function satisfactorily in practice.

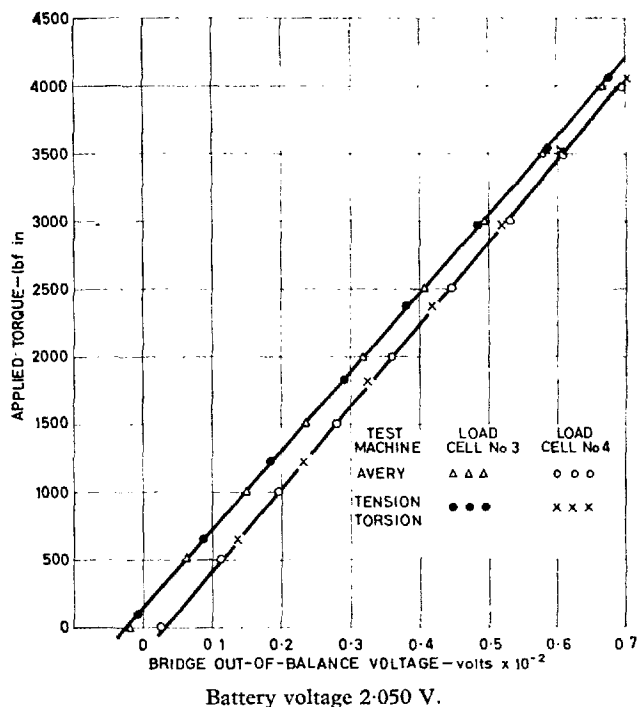
TORSIONMETER CELLS

Two identical torque cells are used and are connected in series on either side of the test specimen. They are 1.15 in diameter and have machined ends identical with the test specimen. They are machined from HE 14T (B.S. 1476) aluminium alloy to give high strength and large magnitude strains for accurate determination of the applied torque. Two paired element torque gauges are bonded to the surface of the cell on the opposite extremes of a diameter and are so arranged that any tension in the cells produces zero out-of-balance in the torsionmeter gauge circuit. The four elements of the two torque gauges on each cell again form a Wheatstone bridge circuit and

hence are automatically compensated against temperature fluctuations.

The torsion cells were originally calibrated in a 15 000 lbf in Avery torsion machine and were checked after assembly in the tension-torsion machine. The characteristics are shown in Fig. 29.8.

Photographs showing the general features described are given in Figs 29.9 and 29.10.



Battery voltage 2.050 V.

Fig. 29.8. Torque cells: comparison between load cell calibration characteristics

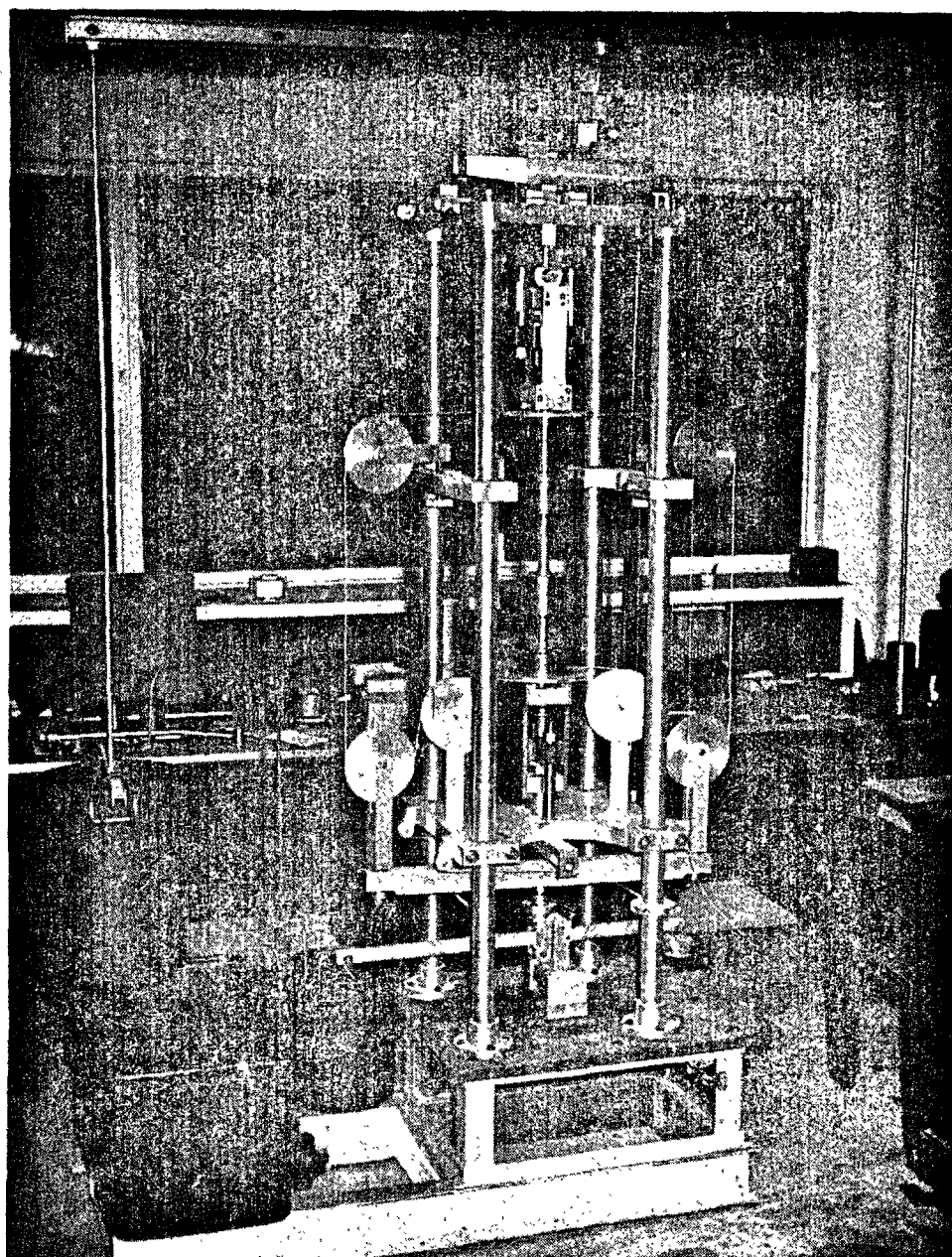


Fig. 29.9

INTERNAL PRESSURIZING OF TEST SPECIMENS

The two end dowels (25) located at the ends of the specimen can be made as one part with a stepped stem. It can be bored throughout to give load balance at the ends when pressurized. The large ends can be grooved to take O-rings for oil sealing together with 'lock-head' end plugs. The linking block (53) can be suitably drilled and connected through a high-pressure flexible pipe to a high-pressure feed.

EXTENSOMETERS

Three extensometers were originally designed and two of them have been adopted for the current tests.

It is envisaged that the temperatures are not likely to exceed about 500°C, whereas the furnace is capable of temperatures exceeding 900°C. Hence all parts are machined from En 58B steel. In case of higher testing temperatures the parts can be made from nimonic alloys.

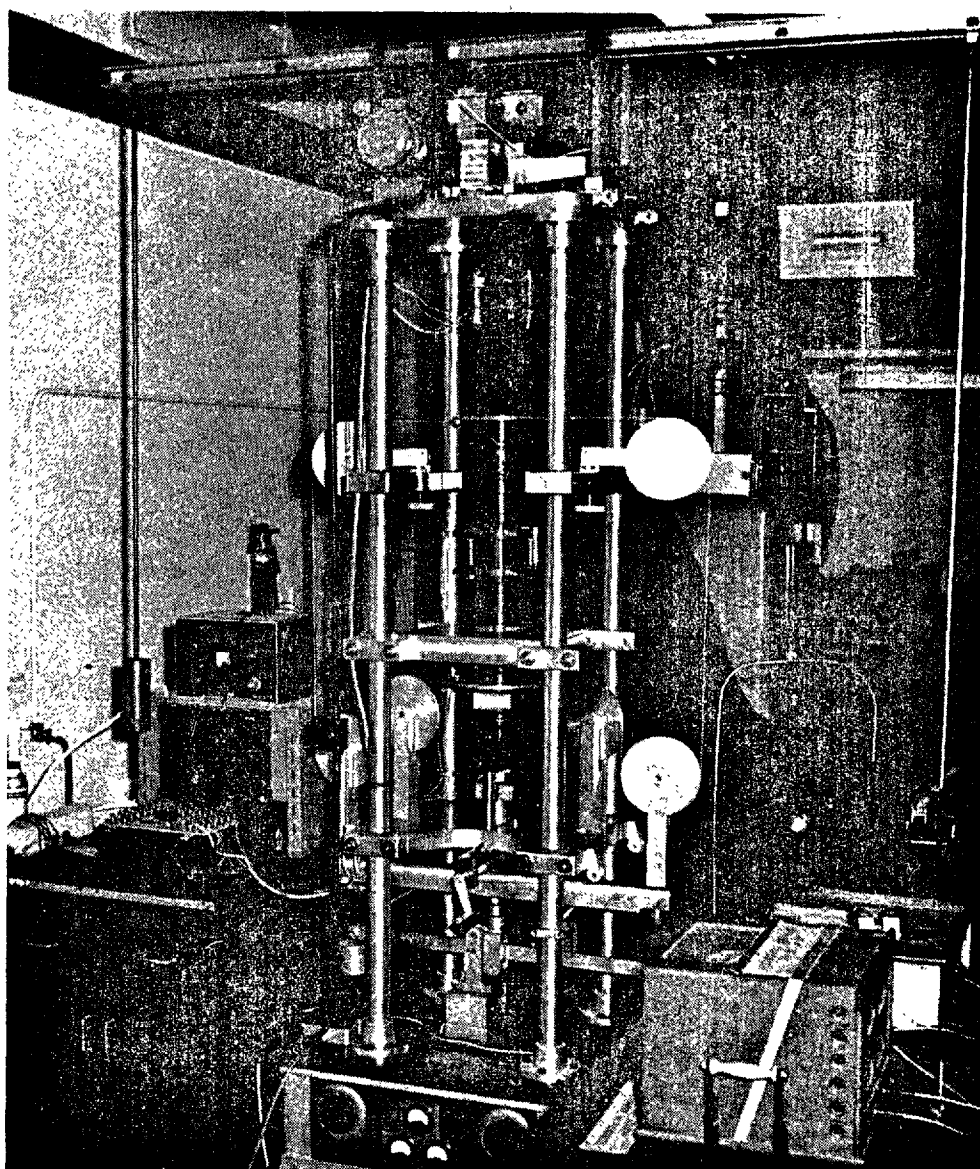


Fig. 29.10

Extensometer No. 1 (to be used in the machine and enclosed within the furnace)

With reference to Fig. 29.11, two cylindrical rings $\frac{3}{4}$ in wide and $2\frac{1}{8}$ in outer diameter are located at the ends of the 4-in gauge length. There are three pick-up probes, shown in the sectional end view, equi-spaced in each ring to give a three point location on the specimen periphery. The probes are spring loaded and are suitably grooved to locate on to the ridges machined on the specimen at the extremities of the gauge length. The springs are also made of En 58B steel. The groove and ridge location was adopted to avoid piercing the specimen and hence to avoid points of high stress concentration.

The displacement measurements are obtained by a grid pointer technique developed by Mair (2). One of the rings is polished and a grid is engraved on its surface. The axial displacement of the lines is equivalent to 4 minutes of rotation while the peripheral lines are 0.002 in apart. The engraving technique used was that developed by Brewer and Alexander (4). Every fifth line is double lined to ease counting and recording. The other ring carries a dove-tailed pointer which scans the grid surface. The position of the pointer is photographed by an auto-recorder at suitable intervals.

By reference to previous photographs the increments of rotational and axial displacement can be derived and logged.

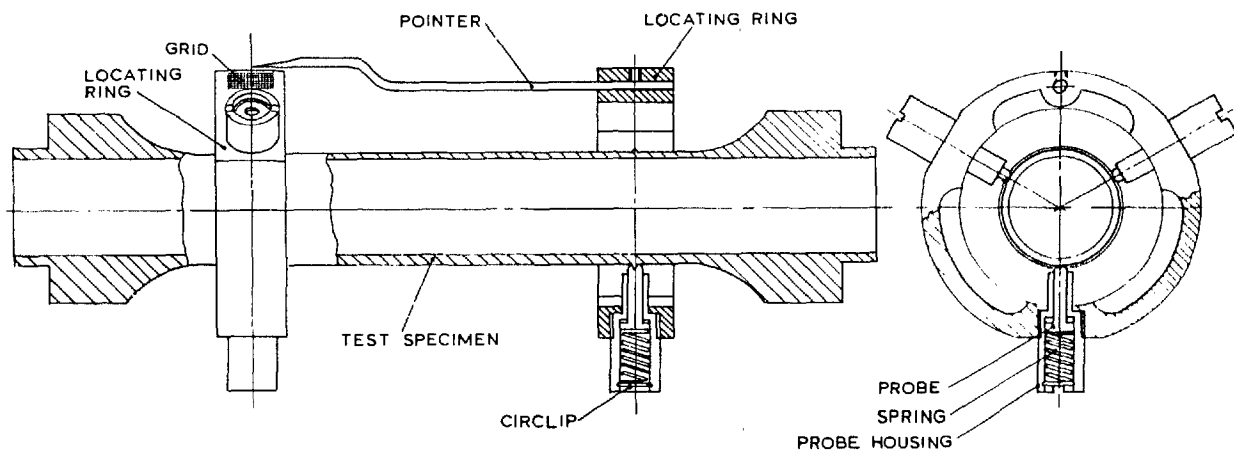


Fig. 29.11. Extensometer assembly—Extensometer No. 1

Extensometer No. 2

This extensometer was designed for use in the conventional tensile creep testing machine. It was needed for hot elastic data of the test materials as well as checking anisotropic properties. The standard specimen was modified (see Fig. 29.12). The threaded ends were spigotted and the anchoring rods counter-bored for alignment. Two ridges identical with those on the specimen shown in Fig. 29.2 are machined at 1 in gauge length. One is picked up by two slotted jaws attached to $\frac{1}{4}$ in by $3\frac{1}{8}$ in En 58B steel bars. The bars at the other end are fixed rigidly to two housings which enclose ball-bearing cages. The bearings allow the housings to float axially along the anchoring rods. The upper housing has two transducers fixed to its

two extremities and their probes rest on two adjusting screws located in the lower housing. By adjusting the screws the initial transducer setting is obtained. The movement between the gauge length registering jaws is transmitted to the housings, and can be transferred on to an auto-recording chart. Tests carried out in simple tensile creep have shown that the extensometer gives a very high degree of accuracy.

ELECTRIC FURNACE

The design of an electric furnace depends on three main factors:

- (1) The type of machine and the test specimen.

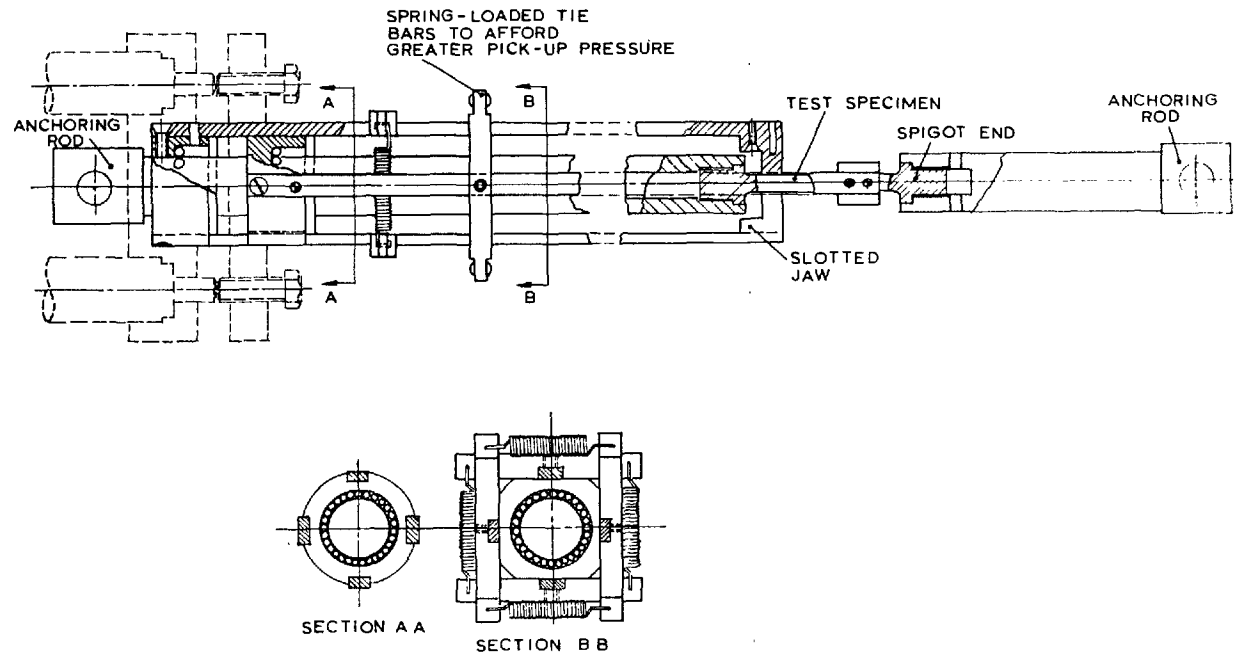


Fig. 29.12. Extensometer No. 2

(2) The cubic capacity of the furnace inner core air space.

(3) The temperature range.

A survey of possible furnaces was undertaken and it was interesting to note that some approximate empirical relationships appear to exist.

The overall heating element length varies from 1.6 to 1.75 of the overall specimen length depending upon the size. The air space core diameter is from six to eight times the diameter of the specimen. The kVA rating is 1.0 kVA per 250 in³ of core air space capacity for temperatures up to 500°C. The maximum temperature depends upon the density of the distribution of the heater windings.

In order to obtain and maintain a uniform temperature it was found that a winding distribution approximately as shown in Fig. 29.13 (plotted to a base of furnace length)

		Starting from lower end		
		Length along former, inches	Windings per inch	Total windings
Length = 362.0 in (R_1) resistance = 50.5 Ω .	Zone R_1	0 - 1½	6	9
		1½ - 3	5	7½
		3 - 3½	4	2
Length = 332.0 in (R_2) resistance = 46.2 Ω .	Zone R_2	3½ - 4	4	2
		4 - 7	3	9
		7 - 8½	4	6
Length = 429 in (R_3) resistance = 59.7 Ω .	Zone R_3	8½ - 9½	0	0
		9½ - 11	4	6
		11 - 13	5	10
		13 - 14	6	6

$$\frac{1}{R_0} = \frac{1}{R_1} + \frac{1}{R_2} + \frac{1}{R_3} = \frac{1}{50.5} + \frac{1}{46.2} + \frac{1}{59.7} = 0.0582 \Omega.$$

R_0 = Overall resistance when zones are connected in parallel = 17.22 Ω .

Length of winding per half turn = 9½ in.

Three zones

Zone length, inches

1st zone 0 - 3½

2nd zone 3½ - 8½

3rd zone 9½ - 14

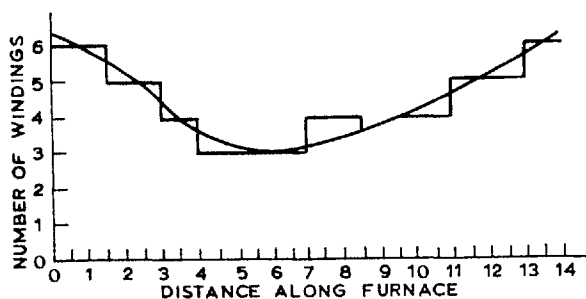


Fig. 29.13

exists in most furnaces. It is essential that all the windings are made from the same resistance wire. A three-zone furnace for uniformity of temperature appears to be common practice. With the winding distribution characteristic shown in Fig. 29.13, uniformity of temperature over the specimen length is assured. For a maximum operating temperature of from 200°C to 800°C for a given furnace it is found that the following empirical rule applies:

$$T^2 = aW_D$$

where T = the maximum operating temperature in degC,

W_D = winding distribution density at the centre of the furnace,

and a = constant.

The furnace constructed is made up of two semi-cylindrical shells which on assembly form a full cylinder. It has an inner bore of 6½ in and is 15 in in length overall. The inner shell is a split tube of the stated dimensions and ⅜ in thick En 58B heat resistant steel. It is flanged along all its edges and the two halves can be fitted together. The outer casing of the furnace is made from mild steel plate of similar thickness. The shells and outer casings are mutually joined by vertical plates and are rigidly fixed in position on semicircular Sindanyo bases. So assembled they form two half-cylindrical annuli. These halves are hinged together along one of the vertical edges and have fastening screws along a vertical edge diametrically opposite to the hinge. The hinge allows the furnace to be opened while the test specimen can be placed in position, removed, or adjustments can be made to extensometers. The windings are fixed on to the surface of the inner shell within the annulus. The detailed construction is described below under the heading 'Windings and their distribution'. Five pairs of holes are made diametrically opposite, radially along the axis at equal distances. They carry silica sheaths for the probes shown. The probes are made from high-temperature steels and travel radially against micrometer heads. The micrometer heads are rigidly mounted on the outer casings. When a steady temperature has been attained, the variation in the specimen cross-section along its axis can be observed from the initial and final readings of the opposite micrometers.

The furnace is assembled and positioned on a mild steel support plate. This plate is bolted to two cross-beam supports similar to (53) of the main machine. The whole assembly is thus free to slide along the columns of the machine if required. The Sindanyo base of the furnace can be bolted to the support plate, hence the requisite alignment of the furnace with respect to the specimen is assured. An inspection port is provided in the furnace wall to view the grid-pointer system of the extensometer. Two light sources are also arranged within the furnace, one on either side of the inspection port, to illuminate the grid.

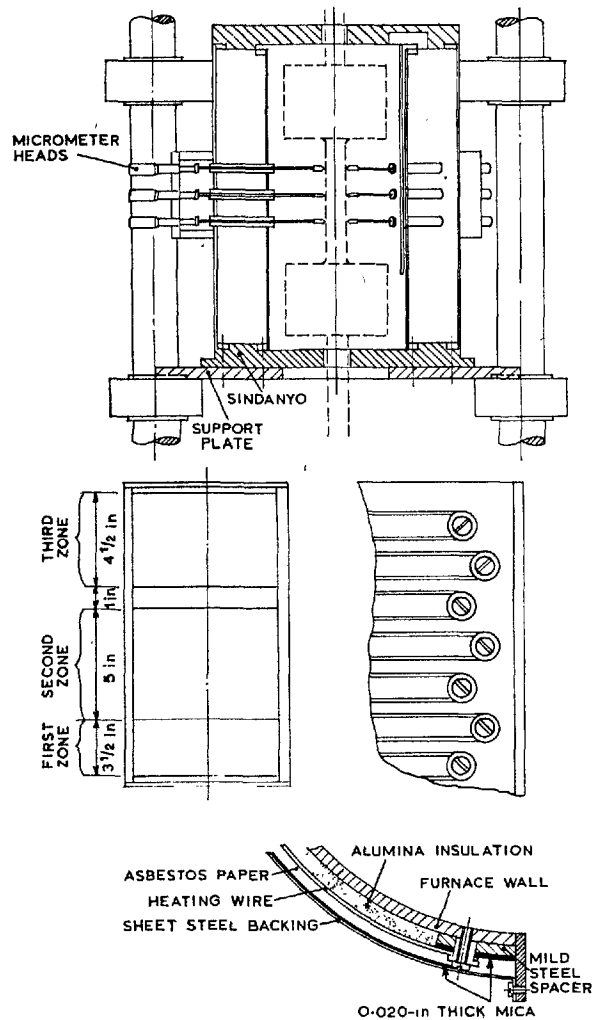


Fig. 29.14. Layout of furnace: heating element fixing

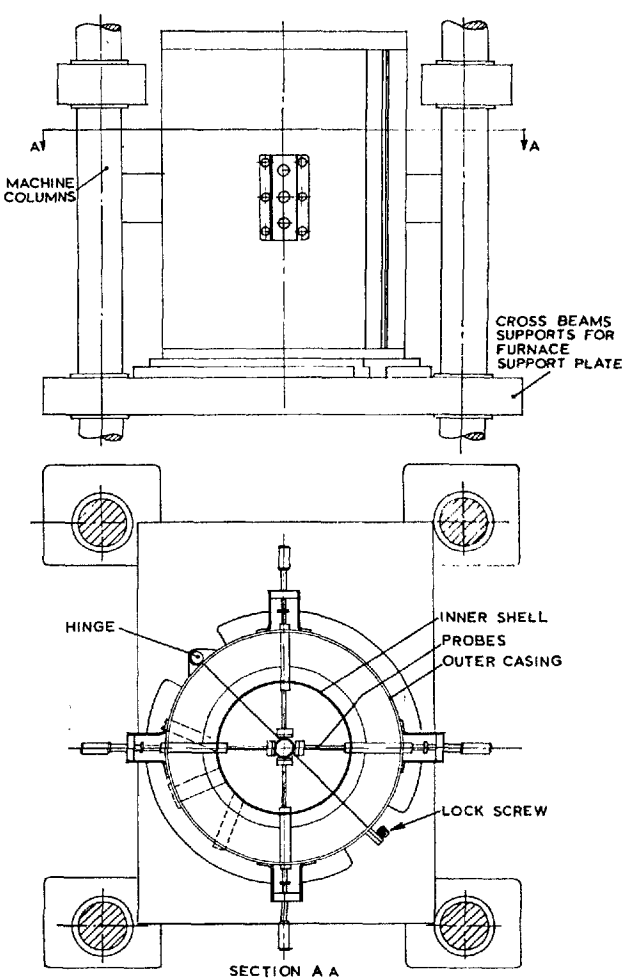
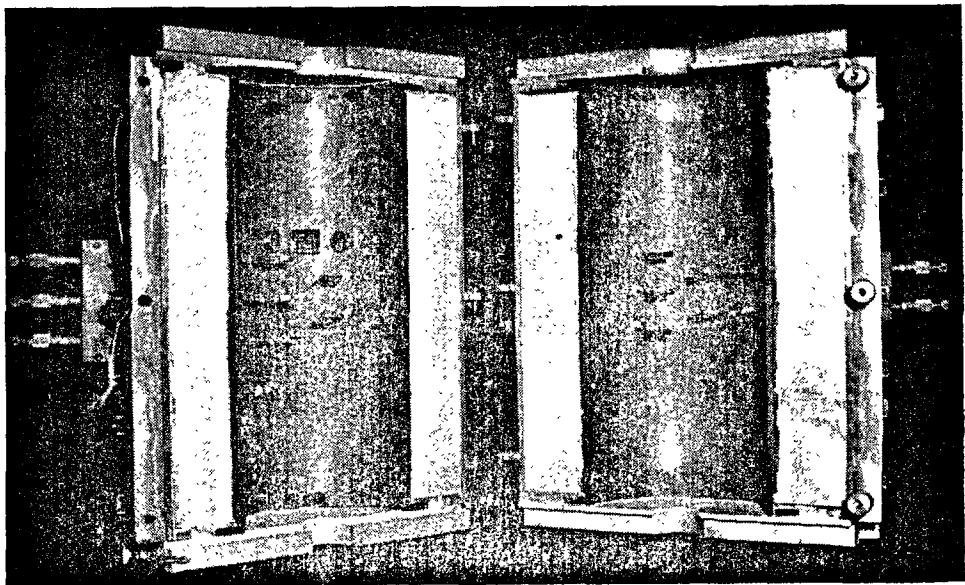


Fig. 29.15. Furnace



Windings and their distribution

On the estimates previously stated the capacity of the furnace was rated at 2 kW to give a maximum temperature of 800°C and a 2½ kW capacity was finally chosen. A three-zone winding was adopted to give an even temperature distribution along the specimen gauge length. A saturable reactor controller was used, with an output voltage of 190 volts, giving a maximum current of 13 amps. The three windings are connected in parallel to the controller. On the assumption that all zones have the same resistance, the effective overall resistance must be 15 ohms, i.e. each zone must be at least 45 ohms.

The winding distribution density was determined and is shown in Fig. 29.14. Each turn has an effective length of 19 in. 'Bright-ray C' material 25 s.w.g. was chosen in view of winding temperature and current-carrying capacity. It has a specific resistance of 1.67 ohms/ft at 20°C. The effective resistance is 17.22 ohms.

In a split furnace, fixing and insulating the windings presents considerable problems. Finally, the fabrication shown in Fig. 29.14 as a scrap view was adopted. A vertical mild steel spacer strip was screwed to the edges of the

inner shell. This strip is drilled and tapped at suitable intervals to carry insulating sleeves held in position by 6 B.A. screws. The outer surface of the shell up to the height of the spacer was filled with 'Alumina' paste and maintained at 200°C for 4 hours to harden. The Alumina is grooved to receive the insulated heater windings. The windings are looped over the sleeves as shown and braced in position by asbestos paper and red mica sheet and finally reinforced by a steel backing sheet to the end flanges. The construction has proved quite satisfactory. Electrical connections to the zones are brought out to terminals on the outer casing along the hinged edges. A sectional drawing of the furnace is shown in Fig. 29.15.

A photograph of the interior of the furnace, showing the probes used for measuring diametral changes, is given in Fig. 29.16.

Furnace control

A circuit diagram is given in Fig. 29.17. The 230-volt a.c. supply is connected to the saturable reactor which can be controlled for any temperature setting to within ± 1 degC by a platinum thermometer and controller. The stabilized supply from the reactor is 190 volts. The second (middle) zone is directly connected to the reactor while the end zones are also connected to the same terminals through two I.K.V.A. Variacs. To gain an even temperature distribution the end zones can be controlled by Variacs.

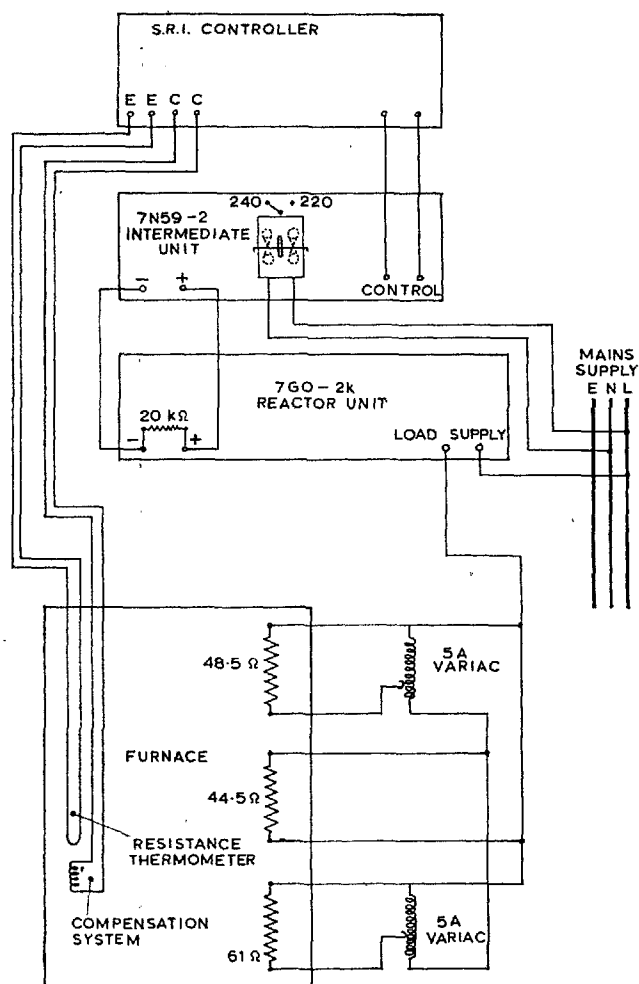


Fig. 29.17. Circuit diagram of furnace control

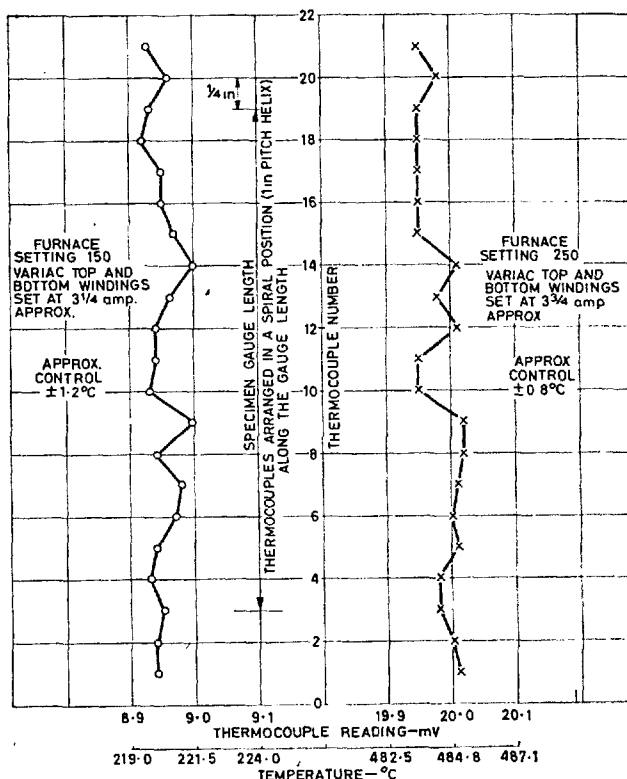


Fig. 29.18. Temperature distribution along dummy specimen

Furnace temperature calibration

To obtain a constant temperature over the specimen gauge length a series of holes 0.040 in diameter were drilled into the walls of a dummy specimen. These holes were arranged in a spiral fashion over the 5 in parallel shank and pitched 0.25 in apart axially. Copper-constantan, glass covered, and 0.010 in diameter thermocouples were peened into these holes and brought out from the top of the furnace to a 24-pole terminal box. The e.m.f. across the thermocouples is measured by a 'Caprico' potentiometer and a melting-ice cold junction.

To calibrate the furnace the dummy specimen as assembled is placed inside the furnace and the saturable reactor switched on. With the temperature controller at a predetermined setting, by adjusting the Variacs and waiting for steady conditions to prevail, thermocouple readings are taken. These are plotted in Fig. 29.18 to a base of specimen length. The Variacs are adjusted successively to obtain the most uniform temperature distribution.

For each different temperature setting of a given test, the above procedure has first to be adopted, using the dummy specimen.

SENSITIVITY AND DEGREE OF CONTROL

(1) *Machine*: Considering the tensile loading system a series of readings was taken and it was found that the least weight that could be easily recorded by the load cells was about 0.025 lb. This gives a load of 2.4 lbf on the specimen, hence a stress of about 12 lbf/in². On the torsion cells it

was possible to detect a torque of 1½ lb in, i.e. an induced shear stress of approximately 15 lbf/in².

(2) *Extensometer*: The smallest detectable axial measurements are 0.001 in over, say, 4 hours, i.e. a strain rate of about 10⁻⁶ in/in min.

(3) *Furnace control*: It was found possible to control the furnace temperature to within ±1 degC while the constancy of temperature is to within ±½ degC over the gauge length.

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APPENDIX 29.1

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