

PARALLEL-LAY ARAMID ROPES FOR USE
AS TENDONS IN PRESTRESSED CONCRETE

by

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Title of Thesis: PARALLEL-LAY ARAMID ROPES FOR USE
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ABSTRACT

A high-performance aramid fibre called Kevlar is beginning to prove its suitability in a wide range of applications. The development, properties and applications of Kevlar are described.

The effect of rope structure on the properties of Kevlar ropes is discussed. It is revealed that the high strength and stiffness of Kevlar yarns are also possessed by Kevlar ropes when the yarns are arranged in a parallel manner. A range of parallel-lay ropes called Parafil is manufactured by ICI Linear Composites Ltd. It is shown that Parafil Type G ropes, based on yarns of Kevlar 49, have the potential to best satisfy the criteria for a prestressing tendon.

An extensive experimental investigation into the properties of Parafil most pertinent to prestressing was performed. Tension testing revealed a virtually linear stress-strain curve prior to a brittle failure. Stress-relaxation tests lasting up to 1000 hours indicated that losses of prestress due to tendon relaxation were not excessive; an empirical relationship was developed to predict long-term results. The behaviour of Parafil in a series of stress-rupture tests showed that static-fatigue would limit the maximum permanent stress in use. The various theoretical approaches to stress-rupture were studied so that rational predictions of long-term behaviour could be made. A cumulative damage technique was developed to maximise operating stresses in prestressed concrete.

The distribution of tensile strength for 6 tonne ropes was determined both theoretically and experimentally. Batch effects, the effect of rope length, and the effects of stretching around a deflector were evaluated statistically.

A full-scale beam test incorporating a single 60 tonne Type G Parafil rope as a prestressing tendon was performed. The integrity of the prestressing system was confirmed. There appeared to be no departure from existing theory in predicting beam behaviour.

Interim guidelines for engineers governing the use of Parafil Type G tendons in prestressed concrete are presented.

To my wife Linda
and children Natalie and Joseph.

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GLOSSARY OF TEXTILE TERMS

In some chapters of the thesis it is occasionally necessary to refer to terms used by textile technologists. In most cases these terms are defined as they appear but for engineers who are unfamiliar with such terminology the following short list may still prove useful.

DECITEX	A metric unit of fibre or yarn size defined as the weight in grams of 10 000 metres of material.
DENIER	A traditional unit of fibre or yarn size defined as the weight in grams of 9000 metres of material. The tensile strength of a fibre or yarn is commonly expressed in grams per denier - see TENACITY.
FILAMENT	A fibre of extreme length manufactured by an extrusion process.
LAY	The length of a complete turn of a yarn or strand in a rope.
PARALLEL-LAY ROPE	A rope in which the yarns are laid parallel to each other in the direction of the rope axis i.e. the yarns are not twisted together.
STRAND	An arrangement of fibres or yarns which may be either twisted or impregnated with resin to form a single unit; strands form the building blocks of many types of rope.
TENACITY	The breaking strength of a yarn or fibre expressed in grams of force divided by the denier (gm/denier). It can easily be shown that: $1 \text{ gm/denier} = 88.26 \times \text{S.G.} \quad \text{MPa}$ where S.G. is the specific gravity of the material. For Kevlar, S.G. = 1.44 and so 1 gm/denier = 127.1 MPa.
TWIST	The number of turns per inch in a yarn.
YARN	A longitudinal group of fibres with or without twist.

CHAPTER 1: INTRODUCTION

The durability of concrete as a structural material has received a great deal of recent attention owing to a growing number of problems. The major concern with structural concrete is the susceptibility of steel reinforcement or steel tendons to corrode. A new material, which does not corrode, has been developed by ICI Linear Composites Ltd. The material, known as Parafil Type G, is a synthetic rope consisting of continuous parallel yarns of the aramid fibre Kevlar 49. It may offer an alternative to steel tendons for use in prestressed concrete.

The availability of a prestressing tendon that does not corrode could have a major impact on the construction industry. For instance, it would free the designer from some of the constraints imposed by steel tendons such as the need to provide concrete cover; tendons could even be placed outside the concrete. Also, it would be possible to use prestressed concrete for applications where the tendency of steel to corrode currently precludes the use of prestressing or makes it possible only if expensive measures are taken to protect the steel. Thus logical applications would be in foundations in chemically aggressive soil conditions, in structures associated with the chemical process industries, in the rapidly expanding field of structural repair, or in structures located in the harsh conditions of a marine environment. The prospects for a non-corrodible prestressing tendon such as Parafil are thus immense.

The major aims of this thesis were, therefore, to assess and develop the potential of Parafil Type G ropes as tendons in prestressed concrete. These objectives were achieved by:

- (1) performing a desk-top study of the properties of Kevlar and Kevlar ropes
- (2) developing a practical prestressing system for the material
- (3) investigating by experiment those properties of Parafil Type G which are relevant to prestressing
- (4) determining safe operating stresses
- (5) providing a practical engineering demonstration of Parafil's ability to provide the prestress to a concrete member

(6) formulating preliminary guidelines to enable engineers to safely design concrete members incorporating Parafil Type G tendons.

The development of Kevlar is first discussed (Chapter 2); the structure of Kevlar and the processes associated with its failure are then briefly described. This work formed an important part of the process of gaining a basic understanding of the core material of Parafil Type G ropes. The majority of Chapter 2 is devoted to an investigation of the existing literature relating to Kevlar 49. From this, the broad characteristics of Kevlar ropes can be inferred and the topics which require further investigation can be identified.

Kevlar ropes of various forms of construction are commercially available. In Chapter 3 the effect of rope structure on Kevlar rope properties is investigated. This helps to identify Parafil Type G as the most suitable Kevlar rope for use as a prestressing tendon. For purposes of comparison, an assessment of the properties of possible alternative prestressing materials is made.

From the point of view of structural efficiency prestressing tendons ought to possess high strength and also be reasonably stiff. An experimental investigation of the tensile properties of Parafil Type G ropes of 60 tonnes nominal breaking load is conducted in Chapter 4; 60 tonne ropes were chosen since they are likely to be the smallest practicable for use as prestressing tendons. Before any tests could be undertaken a system for prestressing Parafil had to be devised; the development of this system is discussed. The development of the main test rig, designed for a variety of tests, is also described.

When designing any prestressed concrete member it is necessary to determine the reduction in prestress that will occur throughout the life of the member; in this manner the amount of extra prestress needed to compensate for the expected losses can be calculated. Chapter 5 begins with a short review of the prestress losses that would have to be taken into account when designing prestressed concrete members incorporating Parafil tendons. Stress-relaxation accounts for a significant loss of stress in prestressing tendons. The results of stress-relaxation tests conducted on 60 tonne Parafil Type G ropes are presented in Chapter 5 and an empirical relationship for determining long-term stress-relaxation is derived.

Chapters 2 and 3 reveal that Kevlar and Kevlar ropes display a stress-rupture phenomenon under ambient conditions in common with many materials. An experimental investigation into the short-term stress-rupture behaviour of Parafil Type G ropes is undertaken in Chapter 6. Two alternative methods for predicting long-term lifetimes of Parafil when subjected to constant loading conditions are presented and discussed.

In Chapter 7 both the theoretical and experimental approaches to stress-rupture in polymers are investigated with the objective of enabling more confident long-term lifetime predictions for Parafil to be made. In order to achieve this objective the case for the adoption of a statistical model derived from the results of tests conducted on Kevlar 49 composite pressure vessels is argued. A cumulative damage technique is developed to enable safe operating stresses for Parafil Type G prestressing tendons to be maximised.

Knowledge of the distribution of short-term strength of a new material is a fundamental requirement for any engineer who wishes to make an initial assessment of the potential of that material. The tensile strength distribution of Parafil Type G ropes is determined both theoretically and experimentally in Chapter 8. The importance of making a comparison between the theoretical and experimental distributions is discussed. The effect of varying engineering parameters such as rope length on strength is assessed by conducting tests and then examining the results statistically.

Great confidence can be instilled in potential users of any new material by demonstrating through practical means that the claims made for that material are entirely realistic. In Chapter 9 the results of two tests conducted on a full-scale prestressed concrete beam incorporating a Parafil Type G tendon are presented. The beam was the first of its type ever constructed.

The conclusions from the research are drawn in the first part of Chapter 10. More importantly, from the commercial viewpoint, interim guidelines for the safe use of Parafil Type G tendons in prestressed concrete are presented. Finally, recommendations for further research are made.

CHAPTER 2: KEVLAR

2.1 Fibre Development

High performance aromatic polyamide or aramid fibres have recently been developed by several companies including E.I. Du Pont de Nemours, Monsanto-Chemstrand and Akzo. The fibres, with a chemical structure based on a benzene ring (Figure 2.1) have a modulus greater than 40 GPa, and so fall within the high performance category (Johnson 1979). This category effectively excludes conventional textile fibres and high strength nylons.

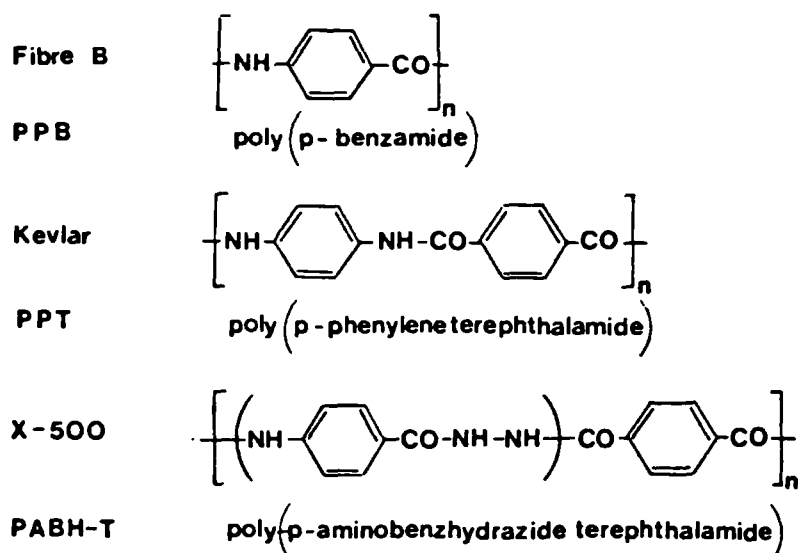


Figure 2.1 Chemical structures of high performance fibres (Johnson 1979).

After the aramid fibre Nomex, poly (m - phenylene isophthalamide), had been successfully spun by Du Pont in 1958, research increased on other aramid fibres by this company and its competitors. A concentrated research effort by Du Pont in particular led to the discovery and announcement of the precursor to Kevlar in 1965 and 1970 respectively. This was known as Fibre B and was based on poly (p-benzamide). The preparation of this fibre is well described by Hughes et al.(1976); it may be produced by either wet or dry-spinning procedures. The

development of Fibre B now appears to have been halted, however, probably owing to the high cost of the starting monomer (p-aminobenzoic acid) and the limited stability of the spinning dope.

The Kevlar polymer, poly (p-phenylene terephthalamide) or PPT, had been prepared as early as 1958 but the existing spinning techniques had failed to produce a high-strength fibre. It was known, however, that PPT polymer dissolved in concentrated sulphuric acid, and so when a technique for spinning from strong acids became available, the preparation of PPT fibres was reconsidered. When the well-known dry-jet wet-spinning process was used in conjunction with a sulphuric acid spinning dope a PPT fibre was produced with properties that surpassed those of previous developments. Furthermore, this new breakthrough also led to increased productivity and considerable cost savings. Hence, the registered name of Kevlar was announced by Du Pont in 1973 to replace that of Fibre B.

Kevlar is prepared by the reaction of terephthaloyl chloride and p-phenylene diamine under carefully controlled conditions. Further details concerning the preparation of Kevlar fibres are given by Carter and Schenk (1975), Hughes et al.(1976), Johnson (1979) and Morgan et al.(1983). The degree of heat treatment given to the washed fibres results in considerable variation in the tensile properties. Consequently, Du Pont produce three different types of the same basic fibre, each intended for specific applications. These fibres are Kevlar, Kevlar 29 and Kevlar 49 (formerly PRD-49).

Kevlar fibres or filaments are a translucent straw colour and have a diameter of 0.012 mm. The material is commercially available in the form of multi-filament yarns of varying size and with a range of finishes. Woven fabric, rovings and staple fibre are also available.

Production of Kevlar, at Richmond, Virginia (USA) began in 1972 and was quickly followed by the production of similar fibres from Akzo (Arenka) and Monsanto (X-500). PPT fibres are now also available in Russia (Terlon) and in Japan. Nevertheless, it appears that Du Pont has captured the major share of the market for this type of fibre. Indeed, production of Kevlar at Du Pont's Richmond plant has now reached 20 million kg/year and could reach 30 million kg/year if the

demand were sufficient. Du Pont de Nemours and Co. has committed more capital expenditure to Kevlar than to any other single Du Pont product.

2.2 Physical Structure

A detailed characterisation of the fine structure of three types of Kevlar has been carried out by Dobb et al.(1977a), using direct electron microscope observations supplemented by X-Ray and electron diffraction analysis. The structure of all three fibres was found to be crystalline and conformed, with only slight differences, with the monoclinic (pseudo-orthorhombic) unit cell. This description is in agreement with that of Northolt (1981). Initial dark-field electron microscope studies, however, suggested major differences in the macrostructure of the various PPT fibres.

A study of the macrostructure of Kevlar 49 in particular, by Dobb et al.(1977b) revealed a supramolecular organisation of hydrogen-bonded pleated sheets arranged radially (see Figure 2.2). The pleat arrangement was found to be absent from the preproduction fibre PRD-49.

Further investigations of the physical structure of Kevlar fibres have been made by Johnson (1979), Morgan et al.(1983) and Schaeffen (1983). Morgan et al.(1983) consider the fibres to consist of extended-chain, rod-like molecules, aligned in the fibre direction. Schaeffen (1983), similarly, refers to them as an organisation of high molecular weight molecules, of high crystallinity, which are extremely well oriented. The dependence of the degree of crystallinity and orientation on molecular weight, in fibres is well-known (Moncrieff 1975). Generally the tensile properties of a fibre improve as the molecular weight increases.

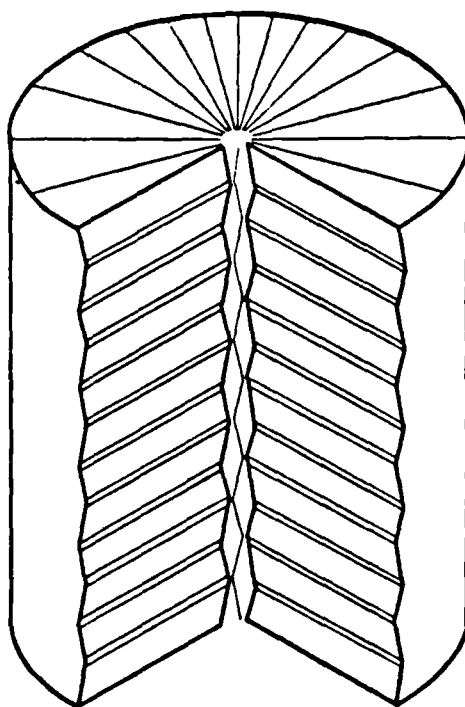


Figure 2.2 Supramolecular structure of Kevlar 49 showing the radially arranged pleated sheets (Dobb et al.1977).

2.3 Failure Processes

Morgan et al.(1983) and Bunsell (1975) have both observed severe splitting in Kevlar fibres that have failed in tension as shown in Figure 2.3. Morgan et al.(1983) observed the split length as being 30-60 times the fibre diameter and also that the extent of splitting was independent of the strain-rate. The average angle that the fibre failure surface made with the fibre axis was only one degree. The two broken ends were found to be different in appearance; while one end exhibited extensive splitting, the other showed a single solid surface. Bunsell (1975) reports that Fuwa (1974) has estimated that as much as three quarters of the strain energy dissipated at failure can be allocated to the creation of fracture surfaces (hence the severe splitting) and non-recoverable plastic deformations. Bunsell (1975) has also observed a similar fracture morphology for preproduction PRD-49 fibres subjected to fatigue loading.

Morgan et al.(1983) propose a model for fibre failure in terms of the distribution of macromolecular chain ends as shown in Figure 2.4. The chain ends are randomly distributed in the fibre skin but are periodically clustered in the core. This results in weak transverse planes every 200 nm, or average PPT macromolecular length, along the fibre axis. The model appears consistent with the crack propagation observed in a single fibre as shown in Figure 2.5; the skin exhibits higher structural integrity than the core which fails more readily through transverse crack propagation.

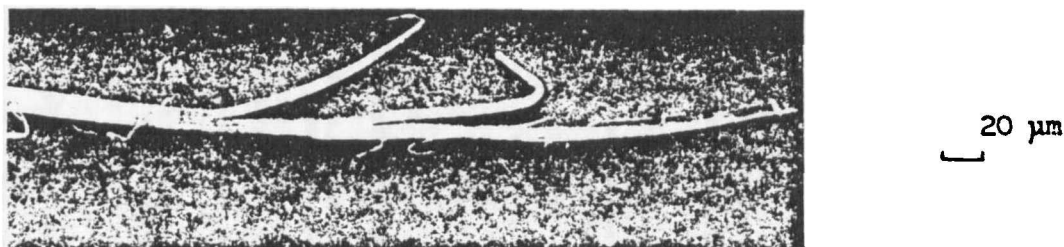


Figure 2.3 Kevlar 49 fibre failed in tension showing severe splitting (Bunsell 1975).

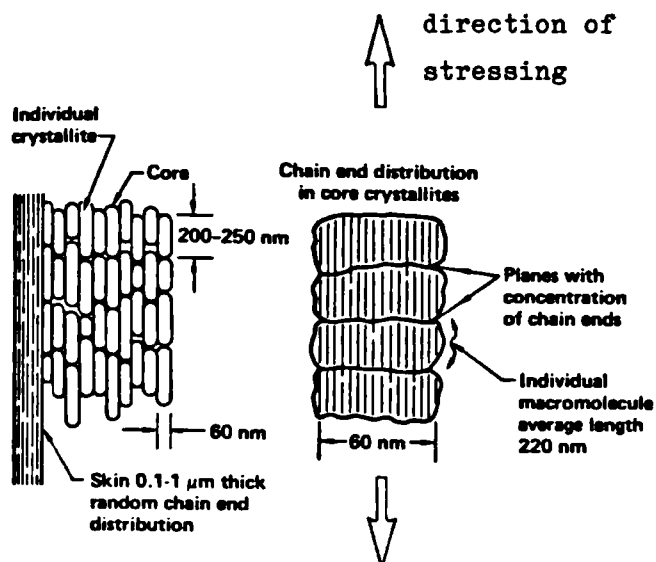


Figure 2.4 Model of the chain-end distribution in PPT fibres (Morgan et al.1983).

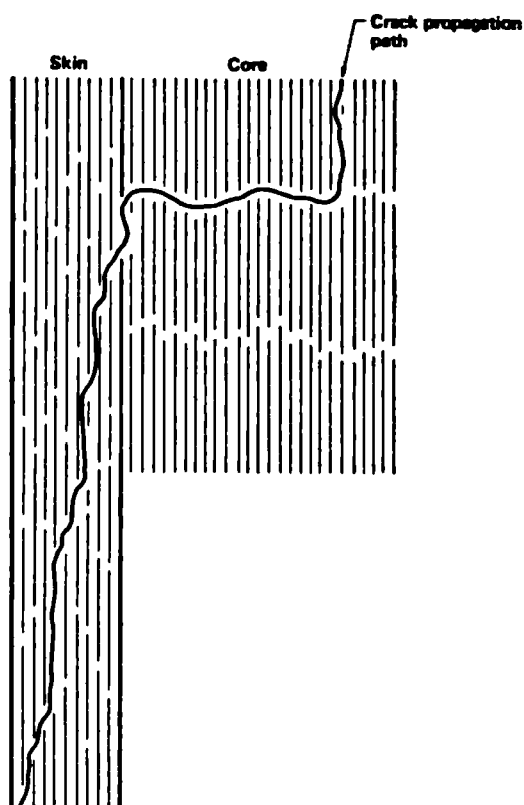


Figure 2.5 Crack propagation path through a Kevlar fibre (Morgan et al. 1983).

2.4 Properties

2.4.1 Introduction

The properties possessed by Kevlar are:-

- high tensile strength
- high stiffness
- high specific strength
- low density
- low creep
- finite life when subjected to high stresses
- good tensile fatigue performance
- good shock-loading performance
- poor compressive strength
- moderate abrasion resistance
- good chemical resistance
- poor ultra-violet resistance
- high melting point
- relatively good thermal stability
- electrical non-conductivity.

Kevlar has been in production since 1972 and so its properties are well documented. There appears to be considerable inconsistency in the results reported in the literature, however, owing to:

- (1) confusion over the particular grade of Kevlar studied
- (2) the lack of differentiation between filaments, yarns, and ropes
- (3) the adoption of a multiplicity of testing techniques.

The detailed accounts of Kevlar properties given by Du Pont (1976, 1981), Ferer and Swenson (1976), Wilfong and Zimmerman (1977), Horn et al.(1977), Schaefgen (1979, 1983) and O'Hear (1983), however, have all originated from Du Pont laboratories or are based on Du Pont published data. Consequently, more consistency in the results reported in these references is observed.

2.4.2 Tensile Properties

The tensile properties of Kevlar yarn and alternative reinforcement materials are shown in Table 2.1.

	Ultimate tensile strength (MPa)	Strain at ultimate (%)	Modulus (GPa)	Specific gravity	Specific strength*
Kevlar 49	2760	2.5	124	1.44	1.92
Kevlar 29	2760	4.0	62	1.44	1.92
Nylon	990	18.3	6	1.14	0.87
Polyester	1120	14.3	14	1.38	0.81
E-Glass	2500	4.0	70	2.6	0.96
S-Glass	4600	5.5	85	2.5	1.84
Carbon fibre	2800	1.0	270	1.75	1.60
Boron fibre	3150	0.8	379	2.49	1.27
Mild steel	300	20.0	200	7.85	0.03
Prestressing steel	1700	1.6	200	7.85	0.22

* specific strength = tensile strength/density.

Table 2.1 Comparative tensile properties.

The specific strength of Kevlar is greater than any other commercially available fibre. Kevlar also has an initial tensile strength greater than steel and a longitudinal stiffness of the same order.

The stress-strain curves for Kevlar 29 and 49 are virtually linear and display negligible plastic deformation prior to failure as shown in Figure 2.6.

2.4.3 Low Density

The low density of Kevlar fibre confers advantages in many applications, particularly in the oceanographic and aerospace markets

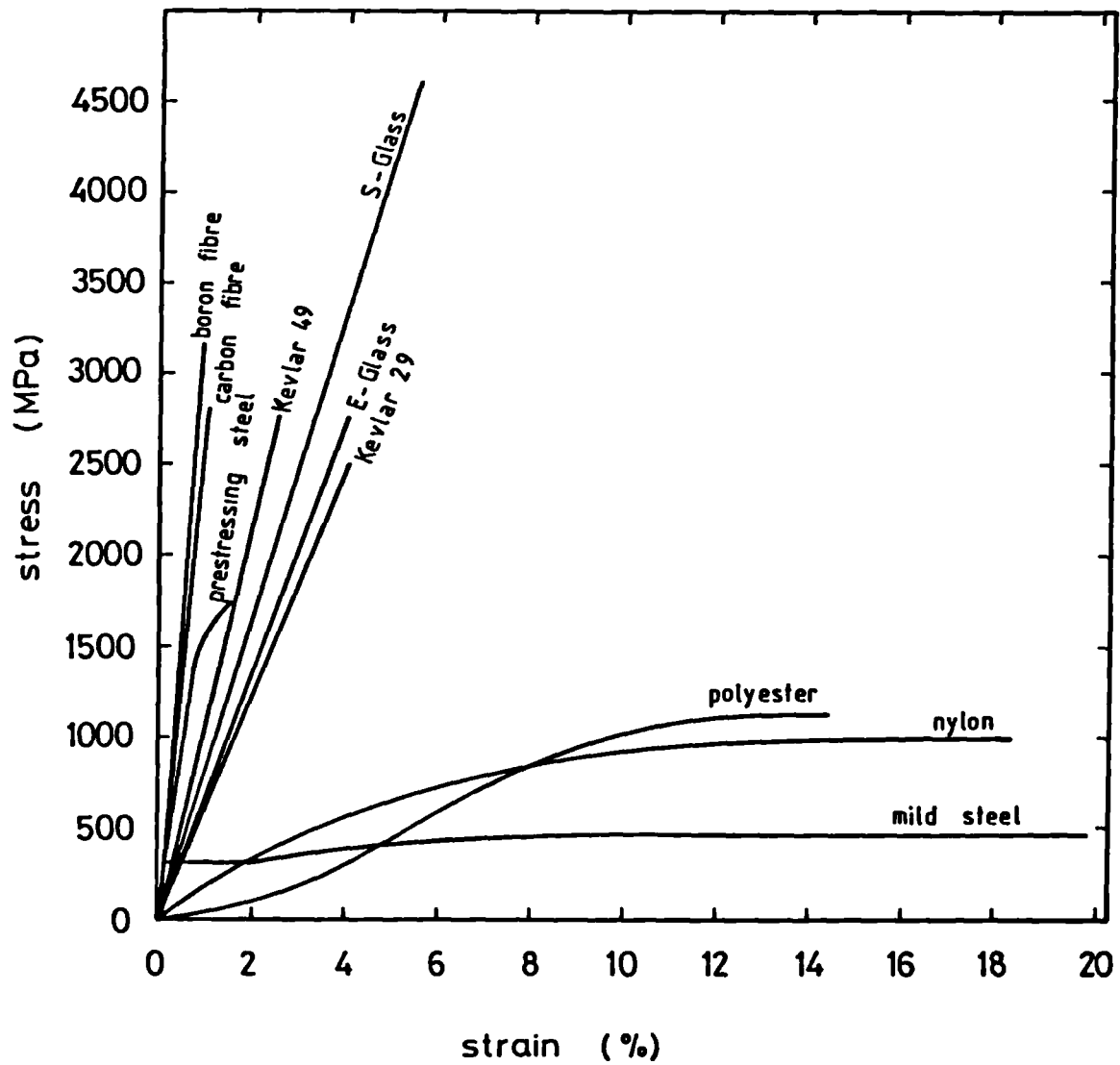


Figure 2.6 Stress-strain curves for Kevlar and alternative reinforcement materials.

where a combination of maximum strength and minimum weight is important. The combination of strength and density for a tension member is often defined by its 'free' length which is the length at which a member breaks under its own weight. Figure 2.7 illustrates the free lengths for Kevlar 49 and steel in air and water.

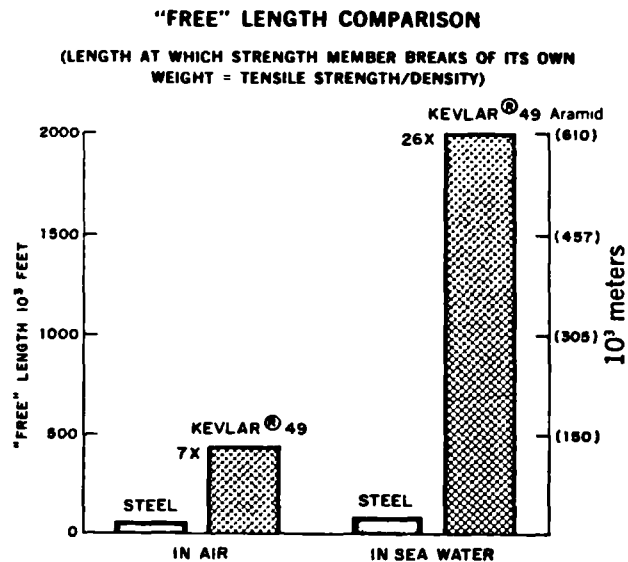


Figure 2.7 'Free' length comparison between Kevlar 49 and steel (Du Pont 1981).

2.4.4 Creep

The creep of Kevlar 49 and Kevlar 29 is generally considered to be a logarithmic function with time. The creep rates for Kevlar are low when compared with other synthetics such as nylon or polyester and approach that of steel as indicated in Figure 2.8. Du Pont (1980) report that the creep rates for yarns of Kevlar 29 and Kevlar 49 are insensitive to load between 20% and 50% of the ultimate load but that they decrease at lower loads. Creep rates of 0.02% and 0.052% per decade were observed at room temperature for Kevlar 49 and Kevlar 29 respectively (one decade is equivalent to one unit on a log time scale to base 10).

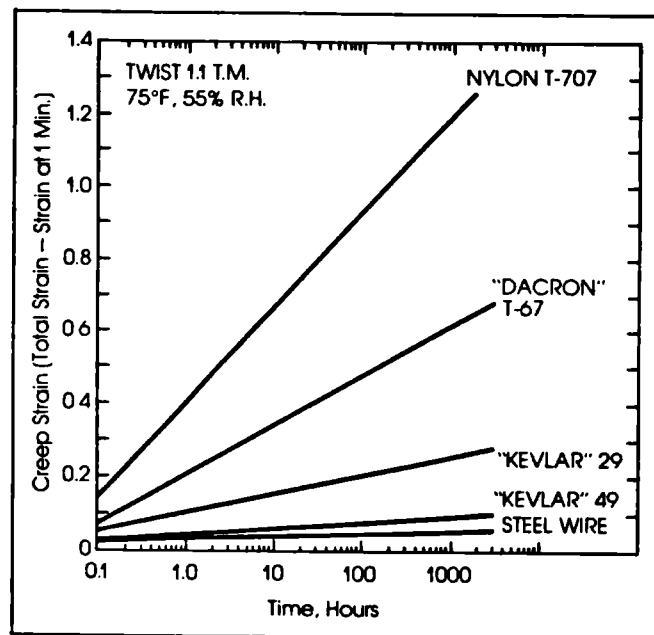


Figure 2.8 Comparative creep behaviour (Du Pont 1980)

Other workers, on the other hand, have found that the rate of creep is stress-dependent. Schaeffgen (1983) refers to work by Blades who observed a linear dependence of the creep rate with stress. Blades defines the creep strain as:

$$\epsilon_c = \epsilon_1 C \log t$$

where: ϵ_c is the creep strain
 ϵ_1 is the strain at one minute (stress-dependent)
 C is the creep coefficient (stress-independent)
 = 0.016 for Kevlar 49
 = 0.024 for Kevlar 29
 t is the time in minutes.

Similarly, Ericksen (1976, 1984) has found that the creep rate for Kevlar displays an increasing trend with stress as shown by Figure 2.9. No attempt was made to resolve the details of the stress dependence in the latter study but it was indicated that the creep

rate tended to exhibit a power law dependence with stress rather than a linear one. Ericksen's work is discussed further in Chapter 6 (6.5).

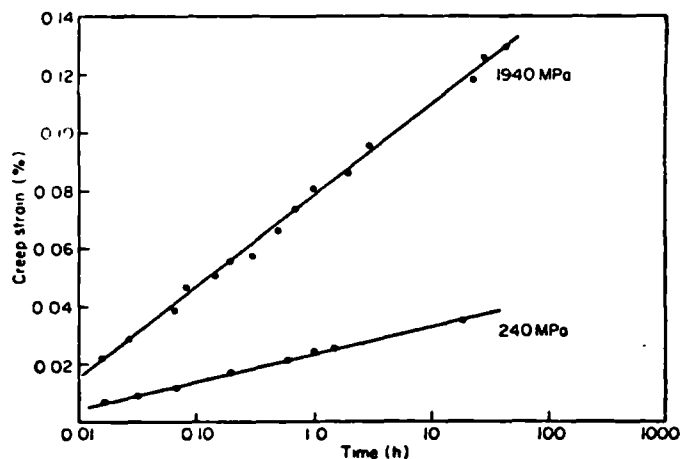


Figure 2.9 Room temperature creep curves for Kevlar 49 fibres (Ericksen 1976).

2.4.5 Stress-rupture

When some materials are subjected to permanently applied loads they eventually creep to failure. This phenomenon is generally referred to as stress-rupture.

Chiao et al.(1974), Glaser et al.(1984) and Howard and Parratt (1985) are amongst many authors who have paid considerable attention to the stress-rupture behaviour of Kevlar. In all cases it was found that Kevlar yarns would support a large proportion of their nominal short-term ultimate loadings, for long periods of time, but that there was considerable variability in the stress-rupture lifetimes for any given load level. The work of these authors is considered in greater detail in Chapter 7.

Wilfong and Zimmerman (1977) have found that the times to failure under dead-weight loading for Kevlar are superior to nylon and

polyethylene. By extrapolating the results of short-term stress-rupture tests on a logarithmic scale with time (see Figure 2.10) these researchers suggest that yarns of Kevlar will support a load of half their breaking strength for over 100 years.

2.4.6 Tension-tension Fatigue

Bunsell (1975), Ferer and Swenson (1976), Wilfong and Zimmerman (1977) and Horn et al.(1977) have all considered the fatigue performances of Kevlar although Bunsell's work was limited to the preproduction fibre PRD-49. A summary of much of the research into fatigue is given in Figure 2.11 where the fatigue performances of Kevlar and other reinforcement materials are indicated in the form of S-N curves. The relative flatness of the S-N curves for Kevlar is an indication of the material's good fatigue performance.

The tension-tension fatigue resistance of Kevlar is superior to steel. Indeed, a zero twist yarn of Kevlar 49 will run for 10 million cycles when stressed between 170 and 1700 MPa whereas the stresses for steel wire have to be confined to the range 100 to 1000 MPa to produce failure after an equivalent number of cycles. As a result, some authors (e.g. Wilfong and Zimmerman 1977) have concluded that tensile fatigue is unlikely to be a limiting design criterion for typical applications of Kevlar.

2.4.7 Impact and Shock-loading Resistance

Wilfong and Zimmerman (1977) report that the breaking strengths for Kevlar at high loading rates are comparable to or slightly higher than those at normal rates. These authors also report that the energy absorption at failure in Kevlar is about half that in nylon, and that the repeated shock loading of a Kevlar rope (up to 40% of its nominal breaking strength) resulted in only a 13% reduction in strength after 100 000 cycles. In addition, Du Pont (1976, 1981) report that Kevlar 29 and Kevlar 49 have performed well in typical ballistic applications.

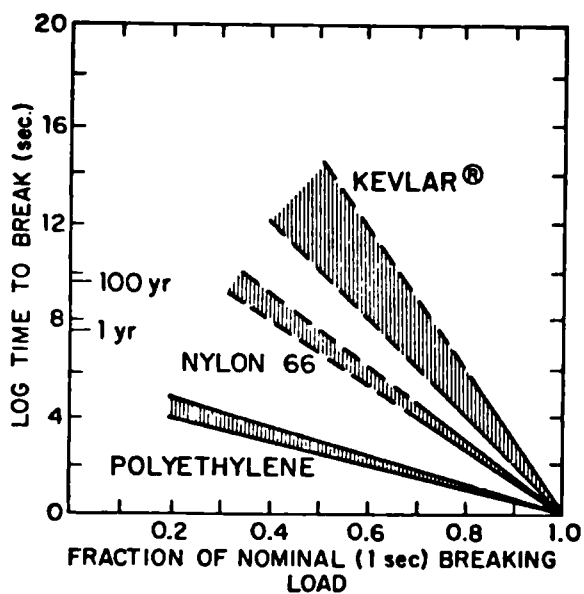


Figure 2.10 Possible stress-rupture behaviour of Kevlar yarns (Wilfong and Zimmerman 1977).

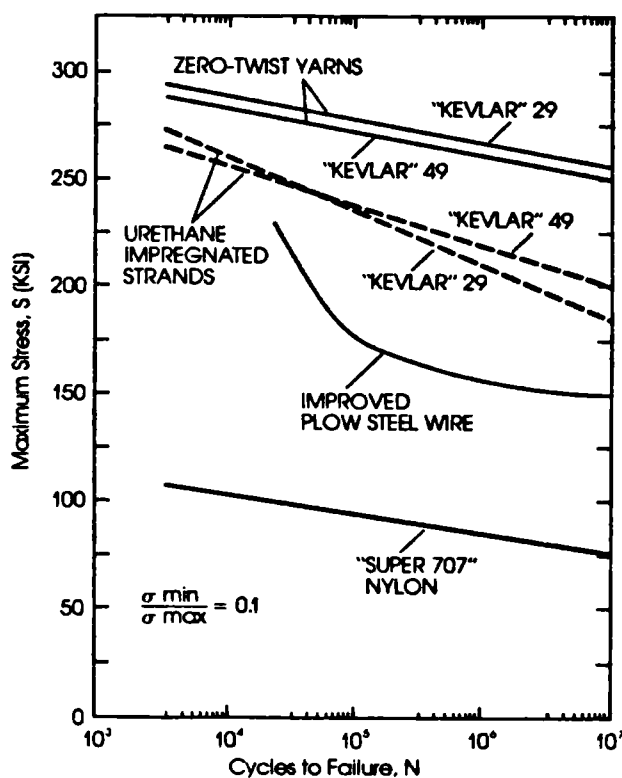


Figure 2.11 Tension-tension fatigue performances (Horn et al. 1977).

Bourgault (1978, 1980) indicates that Kevlar may be prone to shock damage, however, if subjected to a sudden release of tension. The rapid 'snap-back' probably produces a compressive wave and so the damage may possibly be attributed to Kevlar's weak compressive strength (2.4.8).

2.4.8 Compresssive Strength

The compressive strength of Kevlar is likely to be of importance when it is incorporated into a rigid composite, typically with epoxy resin. Wilfong and Zimmerman (1977) and Dobb et al.(1981) report a ratio of tensile strength to compressive strength of about 5:1. This poor performance is apparently due to the low compressive strength of the fibres themselves and not to any matrix component.

Dobb et al.(1981) have studied in depth the behaviour of bare Kevlar fibres in compression. It was concluded that the poor behaviour of Kevlar in compression is due to weak lateral cohesion between the rigid longitudinal molecular chains. The formation of kink bands (Figure 2.12) indicating buckling of the fibrils, was observed using scanning electron microscopy.

Dobb et al.(1981) have also observed a reduction in tensile strength after Kevlar fibres have been subjected to sufficient compression to cause the formation of kink bands. Kink bands may be initiated by flexure (as opposed to uniaxial compression of a stiff composite) and so mishandling of raw filaments or yarns (when flexural compressive strains could be considerable) may be a problem. Figure 2.13 illustrates the significant fall in tensile strength that is observed even after a limited number of flexural cycles.



Figure 2.12 Typical surface banding of the inner surface of a Kevlar 49 fibre (loop-scale marker $4\text{ }\mu\text{m}$) (Dobb et al.1981).

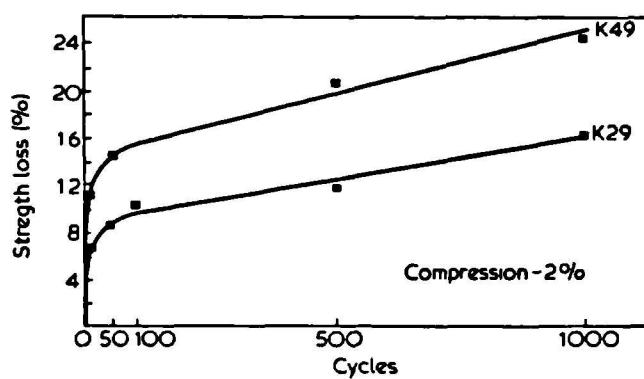


Figure 2.13 Effect of flexural cycles on the tensile strength of Kevlar fibres (Dobb et al.1981).

2.4.9 Abrasion resistance

Horn et al.(1977) recognise that the moderate abrasion resistance of Kevlar may be a potential limitation in some applications. Indeed, the abrasion resistance of Kevlar yarns, in their raw state, is inferior to that of nylon or polyester. However, Ferer (1976) reports improved abrasion performances for Kevlar when a combination of yarn finishes, yarn twist, epoxy impregnation and wax overlay is adopted. As a result, Du Pont now produce Kevlar yarns with two alternative finishes. A 'standard' finish is often applied to aid yarn processing. For rope and cable applications, where abrasion performance is especially important, a special high lubricity finish is available. For applications such as fibre optics, yarns with no applied finish can be supplied.

2.4.10 Chemical and environmental resistance

The chemical resistance of Kevlar is outstanding except for prolonged exposure to some strong mineral acids and alkalis. Tables 2.2 and 2.3 show the effect on strength of some common chemicals.

Du Pont (1980) report no significant loss in tensile strength for both Kevlar 29 and Kevlar 49 after three years exposure to sea-water. Kevlar is degraded substantially, however, when subjected to sources of ultra-violet radiation e.g. sunlight. Table 2.4 indicates the effect of sunlight on the tensile strength of Kevlar 49. Clearly there is some self-screening for the bulkier Kevlar products but nevertheless Du Pont (1981) recommend the application of external jacketing for maximum durability.

Gourdin (1980) has also conducted a study of the 'ageing' of Kevlar in various environments. It was confirmed in this investigation that the loss of strength is small when Kevlar is exposed to sea-water and a variety of chemicals. Significant strength degradation due to ultra-violet radiation and immersion in paraffin were also confirmed.

Chemical	Conc. %	Temp. °C	Time hrs.	Strength Loss, %	
				KEVLAR 29	KEVLAR 49
Hydrochloric Acid	37	21	100	72	63
Hydrochloric Acid	37	21	1000	88	81
Hydrofluoric Acid	10	21	100	10	6
Nitric Acid	1	21	100	16	5
Nitric Acid	10	21	100	79	77
Sulfuric Acid	10	21	100	9	12
Sulfuric Acid	10	21	1000	59	31
Sodium Hydroxide	10	21	1000	74	53
Ammonium Hydroxide	28	21	1000	9	7
Acetone	100	21	1000	3	1
Dimethyl Formamide	100	21	1000	0	0
Methyl Ethyl Ketone	100	21	24	•	0
Trichloroethylene	100	21	24	•	1.5
Trichloroethylene	100	88	387	7	•
Ethyl Alcohol	100	21	1000	1	0
Jet Fuel (JP-4)	100	21	300	0	4.5
Jet Fuel (JP-4)	100	200	100	4	•
Brake Fluid	100	21	312	2	•
Brake Fluid	100	113	100	33	•
Transformer Oil (Texaco #55)	100	60	500	4.6	0
Kerosene	100	60	500	9.9	0
Freon † H	100	60	500	0	2.7
Freon † 22	100	60	500	0	3.6
Tap Water	100	100	100	0	2
Sea Water (Ocean City, N.J.)	100	•	1 yr.	1.5	1.5
Water at 10,000 psi	100	21	720	0	•
Water - Superheated	100	138	40	9.3	•
Steam - Saturated	100	150	48	28	•

*Indicates data not available

† DuPont registered trademark for fluorocarbon

Table 2.2 Chemical resistance of Kevlar (Du Pont 1980).

Environment (100 hrs. exposure at 70°F or 21°C)	Tensile Strength % Retained
ACIDS	
Acetic (99.7%)	100
Hydrochloric (37%)	36
Hydrofluoric (48%)	94
Nitric (70%)	13
Sulfuric (96%)	0
BASES	
Ammonium Hydroxide (28%)	92*
Sodium Hydroxide (40%)	97
SOLVENTS	
Acetone	100*
Carbon Tetrachloride	100*
Benzene	100*
OTHER CHEMICALS	
Gasoline	100*
Salt Water (5% solution)	99.5
Water, Boiling	98

*1000 hrs exposure

Table 2.3 Chemical resistance of Kevlar 49 (Du Pont 1981).

Product Form	Break Load	Strength Retained %
FABRIC*		
Unexposed	300 lb/in. (525 N/cm)	—
Florida Sun, 5 weeks	154 lb/in. (270 N/cm)	51
ROPE, 1/2 in. diameter (13mm), 3-Strand		
Unexposed	14,400 lb (64 100 N)	—
Florida Sun, 6 months	13,000 lb (58 000 N)	90
12 months	11,600 lb (51 600 N)	81
18 months	9,950 lb (44 300 N)	69
24 months	9,940 lb (44 200 N)	69

*Fabric: Plain weave from 380 den (420 dtex) 22 x 22 yarns/in (9/cm)
 4.5 mil (0.1mm) thickness
 2.2 oz/yd² (75g/m²)

Table 2.4 Ultra-violet degradation of Kevlar 49 (Du Pont 1981).

2.4.11 Temperature effects

Du Pont (1976, 1980) have observed only small reductions in the tensile strength of Kevlar up to a temperature of 180°C. Beyond this temperature, however, significant reductions in strength are recorded. Figure 2.14 illustrates the thermal stability of Kevlar 29 when exposed to elevated temperatures up to 250°C for prolonged periods. Kevlar 49 is believed to display similar behaviour. Despite the strength loss at high temperatures Kevlar does not melt or support combustion; indeed, the material may be described as flame-retardant and self-extinguishing.

Wilfong and Zimmerman (1977) have studied the effects of elevated temperatures on tensile strength after short periods of exposure (Figure 2.15). A 70% strength retention was observed at 200°C after five minutes.

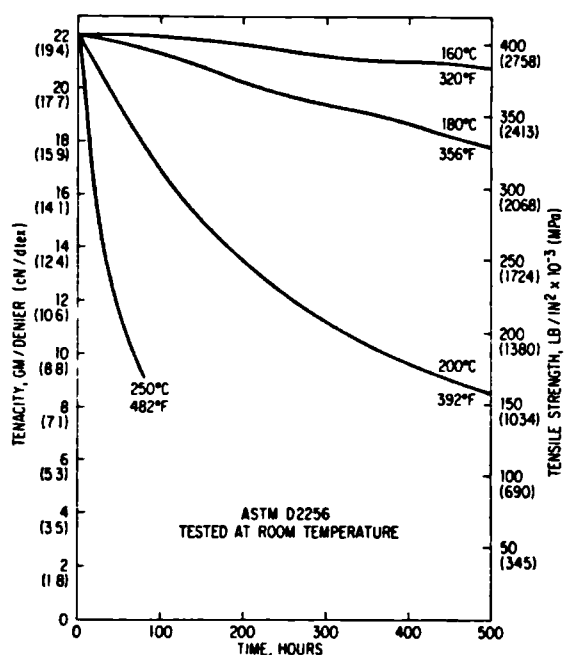


Figure 2.14 The effect of temperature on the tensile strength of Kevlar 29 after prolonged exposure (Du Pont 1976).

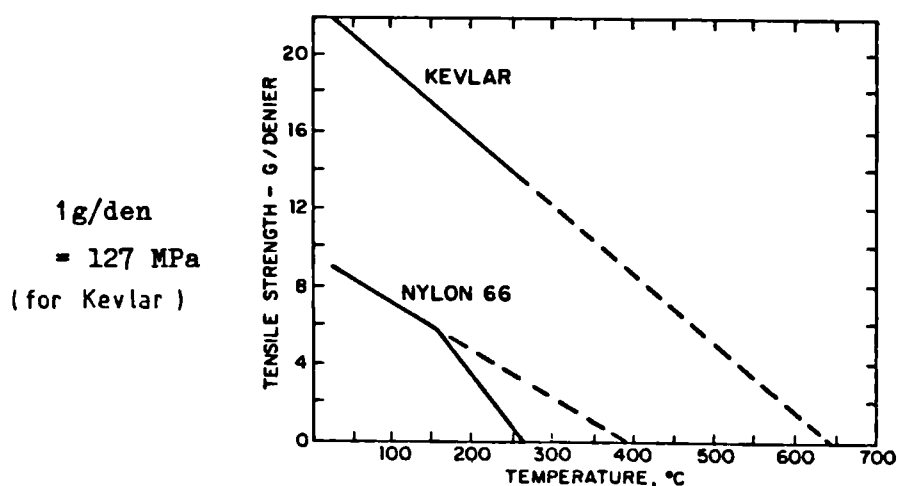


Figure 2.15 The effect of temperature on the tensile strength of Kevlar 29 and Kevlar 49 after five minutes exposure in air (Wilfong and Zimmerman 1977).

At cryogenic temperatures (-170°C) the material does not embrittle or lose its properties. In fact, under arctic conditions a slight increase in the tensile strength and modulus of Kevlar has been observed (Ferer and Swenson 1976).

Gourdin (1980) has also investigated the effects of temperature on Kevlar. Good resistance to thermal shock (-196°C to $+196^{\circ}\text{C}$) is reported. The behaviour when subjected to thermal cycling between -40°C and $+150^{\circ}\text{C}$ is described as moderate.

Table 2.5 (Hancox 1981) lists further thermal properties for Kevlar including other properties dealt with previously. The negative coefficient of axial thermal expansion is likely to have structural significance (7.13).

Property	Units	Kevlar 29 poly(p-phenylene terephthalamide)	Kevlar 49
Tensile strength (mean)	GPa	3.45	3.62
(resin impregnated strand)		3.45	3.62
(dry yarn, twisted)		2.76	2.76
Coefficient of variation	%	—	4
Modulus of elasticity (mean)	GPa	58.6	124.0
Coefficient of variation	%	—	5
Elongation	%	4	2.5
Filament diameter	μm	12.1	11.9
Specific gravity		1.44	1.44
Specific strength	GPa	2.39	2.51
Specific modulus	GPa	40.69	86.11
Coefficient of thermal expansion:			
longitudinal	10^{-6}K^{-1}	—	-2
diametral	10^{-6}K^{-1}	—	+59
Specific heat	$\text{J kg}^{-1} \text{K}^{-1}$	—	1420
Refractive index:			
longitudinal		—	2
diametral		—	1.6
Loss tangent at 10^{10} Hz		—	0.005 i
Dielectric constant		—	3.2 i
Power factor at 10^{10} Hz		—	0.016 i
Volume resistivity	$\mu\Omega\text{m}$	—	10 i
Thermal conductivity 298 K:			
longitudinal	$\text{Wm}^{-1} \text{K}^{-1}$	—	0.57
diametral	$\text{Wm}^{-1} \text{K}^{-1}$	—	0.49
Coefficient of friction:			
yarn-yarn		—	0.46
yarn-metal		—	0.41
Moisture regain 55% RH:			
295 K	%	5	3.5-4.0
Decomposition temperature	K	773	773
Long-term use in air	K	443	433
Chemical resistance: (see Dupont [1])		Attacked by acids	Attacked only by alkalis and strong oxidising agents

Table 2.5 Thermal and other properties of Kevlar filaments (Hancox 1981).

2.5 Applications

Du Pont have produced a considerable amount of trade literature outlining specific applications for Kevlar fibres. Johnson (1979) and Schaefgen (1983) also give details of the areas in which Kevlar is being employed. From the literature it is clear that Kevlar is currently being used: in the aircraft and automotive industries, for general industrial purposes, in the marine industry, for personnel protection, in ropes, cables, fabrics and nets, and also in sporting goods. Table 2.6 indicates the wide usage of Kevlar.

Kevlar is often used in conjunction with a variety of other materials. For instance, Kevlar 49 is particularly useful as the reinforcing material in reinforced plastic composites, widely used in the aircraft industry. Alternatively, it can be combined with graphite to form a hybrid composite which is strong in flexure and also light in weight. It can also be used to form a laminate with timber for use in sporting goods.

The other grades of Kevlar are also being employed widely. For example, Kevlar 29, in conjunction with other materials, is often used as a reinforcing element in many industrial products such as conveyor belts, hoses and tarpaulins. Also, the product designated simply as Kevlar (as opposed to Kevlar 29 or Kevlar 49) is now a widely used material for the belts of automotive radial tyres.

Aircraft/Aerospace

Panels, Flooring
 Escape slides
 Pressure vessels
 Rocket motor cases
 Propellor and helicopter blades
 Parachutes

Automotive

Car body panels
 Truck chassis beams
 (hybrid with carbon fibre)
 Brake linings
 Clutch facings
 Gaskets
 Hoses and Belts
 Belts in tyres

Industrial

Conveyor belts
 Tarpaulins
 Chemical hoses
 Ventilation ducts
 Rotor vanes

Personnel protection

Bullet-proof body armour
 Flak-jackets
 Helmets
 Work gloves
 Nuclear shelters

Ropes and Cables

Mooring lines
 Guy ropes
 Balloon tethers
 Oil rig risers
 Tensioner lines
 Pendant lines
 Electromechanical cables

Marine

Boat hulls
 Rigging
 Canoes
 Coated fabrics
 Sails

Sporting goods

Hockey sticks
 Tennis racquets
 Golf clubs
 Fishing rods
 Skis

Table 2.6 Applications of Kevlar and Kevlar Composites.

CHAPTER 3: ROPES FROM KEVLAR

3.1 Rope structure

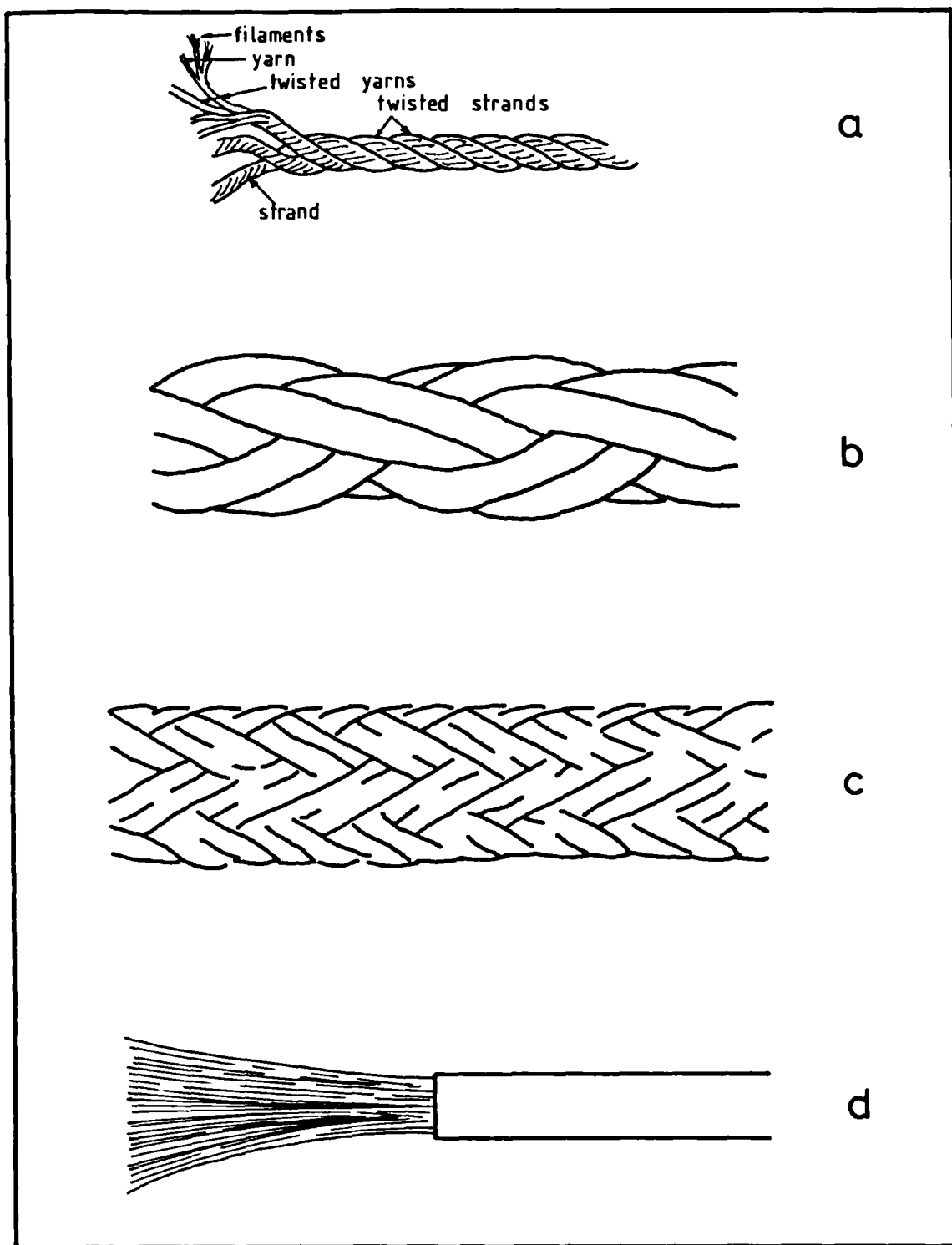
3.1.1 Introduction

Kevlar, in the form of bare or resin impregnated yarns, has proved its suitability for a wide variety of rope and cable constructions. These may broadly be categorised as twisted, braided or parallel-lay (Table 3.1 and Figure 3.1). Bare yarns (unimpregnated) may be processed on conventional textile twisting, stranding or braiding equipment. Traditional steel-wire stranding, cabling and braiding machinery is suitable for handling resin impregnated yarns.

Ferer and Swenson (1976) and Bourgault (1980) have considered the implications of rope structure on properties and on suitability for a particular application. Ferer and Swenson (1976) consider the selection of rope structure to be equally as important as the choice of rope material. Similarly, Bourgault (1980) regards the application of the rope as the main criterion in selecting a particular construction. The choice should optimise the balance of strength, modulus and flexibility.

Twisted	Braided	Parallel
Three-strand Four-strand Cable design Double helical lay	Solid braid Single braid Double braid Plaited	Parallel strand Parallel yarn

Table 3.1 Types of rope construction (Ferer and Swenson 1976).



- a, b twisted construction
 c braided construction
 d parallel-lay construction

Figure 3.1 Types of rope construction.

3.1.2 Twisted ropes

Before continuous yarns were available early ropes could only be constructed using a twisted construction. The twisting enabled sufficient interfibre friction to be developed between short lengths of yarn (or fibre) to allow a tensile load to be transmitted. This type of construction has remained popular, however, even though the development of large frictional forces is no longer necessary. In fact, the degree of friction and the associated problem of interyarn abrasion can cause problems in Kevlar ropes. Hence, it is recommended that when this form of construction is adopted for Kevlar ropes the yarns should be impregnated with resin or coated with a special lubricity finish (2.4.9).

Despite the existence of high transverse forces, twisted ropes display good fatigue resistance and are often referred to as 'working' ropes. Their main disadvantage, however, is that torque is easily induced in the rope when it is loaded which causes rotation if one end of the rope is free to turn. Also, once the load is removed, twisted ropes have a tendency to kink or loop. Successful attempts have been made, however, to produce a torque-free twisted Kevlar rope (Bourgault 1980); the process involves twisting the yarns within individual strands in the opposite direction to that of the strands themselves.

Ropes of twisted construction are designated by two figures in the same way as those of wire-rope construction. For example a 19 x 7 configuration indicates that there are 19 strands with 7 yarns in each strand.

3.1.3 Parallel-lay ropes

Ropes of parallel-lay construction became possible with the advent of continuous yarns. Their construction, however, requires greater care than for twisted or braided ropes, to ensure that the parallel yarns are loaded equally, and hence that the maximum achievable strength is realised. Despite this, parallel-lay ropes usually exhibit the highest strength efficiency (ratio of rope strength to the cumulative strength based on individual members) primarily because abrasion

between yarns is minimised. For the same reason, Kevlar ropes of parallel-lay construction generally display the best performance when cycled in tension-tension fatigue. Another advantage is their good performance in creep-critical applications.

One problem with parallel-lay ropes is that any breakage in a yarn results in a loss of load-carrying capacity throughout the entire length of the rope. There is no facility for load redistribution through interyarn friction. They are, however, torque-free and the problem of kinks does not occur.

3.1.4 Braided ropes

Braided ropes appear to be the most popular owing to their ease of manufacture. The precision machinery needed for twisted or parallel-lay ropes is not required.

Ferer and Swenson (1976) indicate that the properties of braided ropes can be varied considerably by changing the braid construction (Table 3.1) or the braid angle (degree of interlacing). The tensile strength and modulus decrease with increasing braid angle. The strength is reduced because the higher compressive stresses associated with large braid angles give rise to increased abrasion.

Braided ropes offer the advantage of improved flexibility at lower cost. They are supple, easy to handle, and are completely torque-balanced. They are also less susceptible to failure as a result of local damage since sufficient friction can be developed between yarns to maintain the integrity of the rope. Furthermore, braided ropes do not require an external jacket to hold them together although some protection against the harmful effects of sunlight is usually provided.

3.1.5 Manufacturers

Appendix 1 presents a representative group of suppliers of the various types of Kevlar ropes throughout America and Europe. Some basic properties are also included. The names of additional

manufacturers may be obtained from the marketing department of Du Pont de Nemours and Co (Inc).

3.2 Properties

3.2.1 Introduction

A preliminary evaluation of the basic properties of a small number of ropes incorporating Kevlar was made by Du Pont (1974). These results are rather limited, however, since the work was concentrated mainly on tensile strength and the elongation at failure.

Ferer and Swenson (1976) and Horn et al.(1977) have evaluated the properties of Kevlar ropes in more detail including: their tensile properties, their creep and stress-rupture behaviour, their fatigue performances and their abrasion resistance. Wilkins (1975) has also investigated the behaviour of three types of Kevlar 49 tension members. Of particular interest to this investigator was the suitability of Kevlar as the load-bearing element of electromechanical cables. Consequently, the results are not as general as those from the former authors.

O'Hear (1983), in a consideration of the offshore applications for Kevlar ropes, has naturally concentrated on the most pertinent properties. Yarn abrasion and the performance of ropes when cycled over sheaves are considered to be most important in this field.

3.2.2 Tensile properties

Generally Kevlar ropes are about twice as strong as nylon or polyester ropes. They have strengths similar to steel ropes but weigh considerably less; steel ropes are typically four to five times heavier. Appendix 1 gives the tensile strengths of some typical Kevlar ropes.

Figure 3.2 demonstrates the effect of rope size on breaking length; for constant strength the breaking length (for a particular material) should be the same irrespective of rope size. In general, however,

the strength of synthetic ropes decreases with rope diameter (owing to the statistical properties of large bundles). A similar reduction is observed in steel wire ropes but this is largely attributable to the reduction in the beneficial effects of cold-drawing associated with the larger wire ropes. Surprisingly, the parallel-lay ropes show the reverse trend but this is only because the non-loadbearing outer sheath, necessary to encapsulate the yarns, becomes a smaller fraction of the rope weight with large diameters.

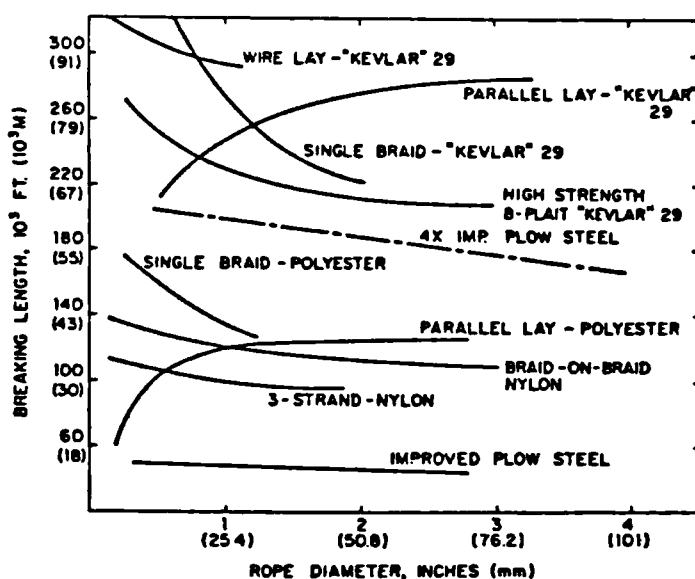
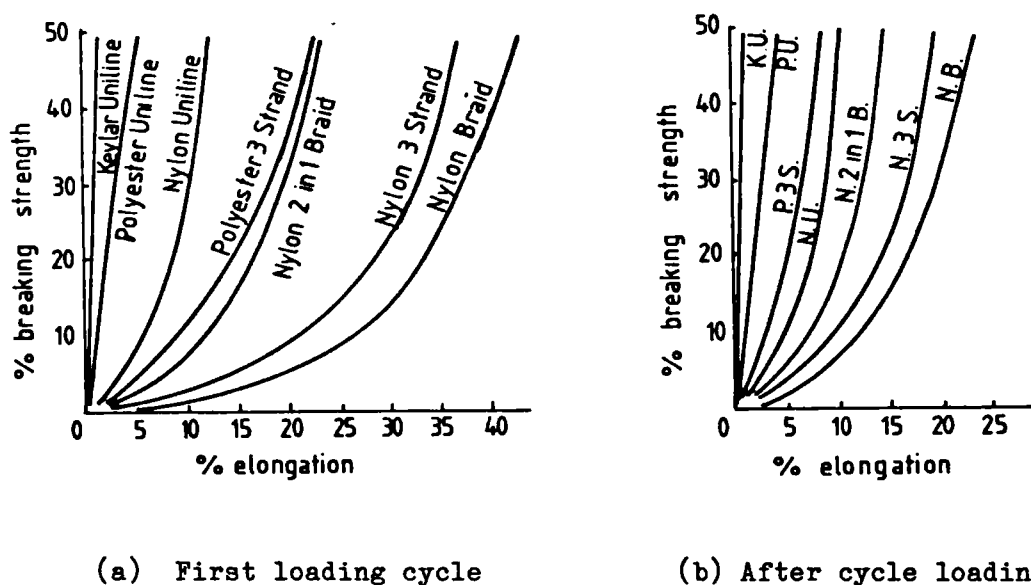


Figure 3.2 The effect of rope size on strength (Horn et al.1977).

When considering the cross-sectional area of Kevlar alone, ICI Linear Composites Limited claim a minimum strength efficiency of 72% for their parallel-lay ropes, independent of the rope diameter. On the other hand, for a 100 tonne Kevlar cable of twisted construction, O'Hear (1983) reports a strength efficiency of 55%, while the strength of a braided rope (braid angle 30°) has been calculated by Ferer and Swenson (1976) to be only 68% of the strength of a rope with the same strands laid parallel.

Figure 3.3 illustrates the load-extension behaviour for a variety of ropes. Ropes of braided and twisted construction display large amounts of constructional stretch before carrying any appreciable

load. The ropes of parallel-lay construction, however, attain their full stiffness almost immediately with the Kevlar rope displaying the highest stiffness. The load-extension behaviour of ICI's Parafil Type G ropes is illustrated in Figure 3.13 (3.5.1) and discussed in detail in Chapter 4 and Chapter 8.



(a) First loading cycle (b) After cycle loading

Uniline is a parallel-lay rope produced by Wall Rope Works, Beverly, N.J., USA.

Figure 3.3 Load-extension behaviour for typical synthetic ropes (Ferer and Swenson 1976).

3.2.3 Creep and stress-rupture

The creep behaviour of ropes may be extremely important in a number of applications. Creep within guy ropes used for masts, for instance, may result in a significant loss of stress. Similarly, excessive creep of the load-bearing element of electromechanical cables often causes problems since it can sometimes lead to failure of the conductor. In extreme circumstances ropes may even creep to failure. This is known as stress-rupture.

Horn et al.(1977) have compared the creep of some Kevlar ropes with that of the individual yarns from which the ropes are made. The creep behaviour of single yarns is regarded by these authors as the optimum

that could be achieved in ropes owing to factors such as: rope structure, the manufacturing process and the types of termination employed. The results are shown in Figure 3.4. As expected the parallel-lay ropes approximate most closely to the yarn behaviour while the braided ropes display large creep rates owing to their type of construction. Figure 3.5 illustrates the creep curve for ICI's parallel-lay Type F rope based on Kevlar 29; an approximate creep rate of 0.04 % per decade was observed which compares reasonably well with the 0.05% per decade reported by Horn et al.(1977) for a rope of similar construction (Figure 3.4). Hence, it is likely that ICI's Type G Parafil ropes based on Kevlar 49 will exhibit lower creep rates. Indeed, Horn et al.(1977) recommend parallel-lay ropes incorporating Kevlar 49 yarns as the preferred material in creep-critical applications.

Research on stress-rupture has mainly been concentrated on Kevlar yarns. Little attention has been devoted to determining the stress-rupture behaviour of Kevlar ropes although the two are obviously related. The lack of research into rope behaviour may be because a large number of applications involve short-term loading only, or because end-users prefer to avoid the issue by keeping operating stresses excessively low. Ferer (1977), however, reports the results that were obtained for a 0.25 inch diameter Uniline rope manufactured by Wall Rope Works (USA). (Uniline is a parallel-lay rope which has now been superseded by Miniline, a rope of low braid angle.) The work was sponsored by the American Navy who wished to establish long-term safe mooring loads. Figure 3.6 indicates the results obtained. Ferer (1977) recommends a maximum tensile load of 70% of the initial breaking load for long-term use; this contrasts markedly with earlier work (Ferer and Swenson 1976) in which a safety factor of 5:1 was recommended for all aramid ropes. A closer inspection of the results, however, reveals that 70% is probably optimistic. In fact, although Wall Rope Works include the same figure (i.e. Figure 3.6) in their trade literature for Miniline, the Company suggests that a stress level equal to 25% of the initial ultimate stress is realistic for long term loads while an absolute maximum of 40% is recommended for even short periods of time. The stress-rupture behaviour of ICI's Parafil Type G ropes, similar in construction to Uniline, is considered in detail in Chapter 6 and Chapter 7.

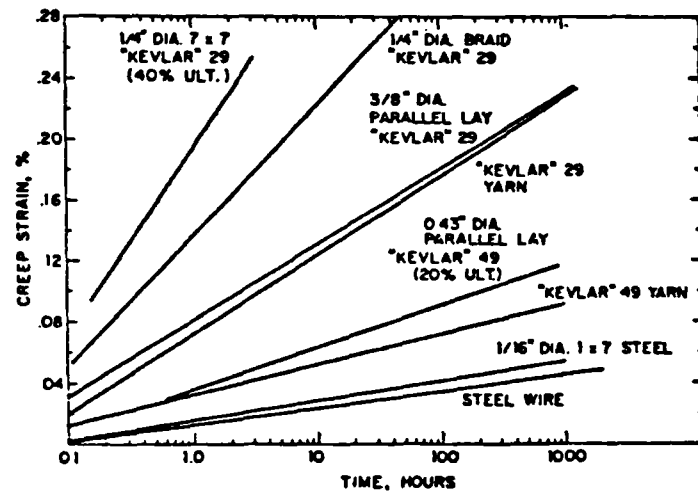


Figure 3.4 Creep of ropes compared with yarns at 50% of ultimate strength (Horn et al.1977).

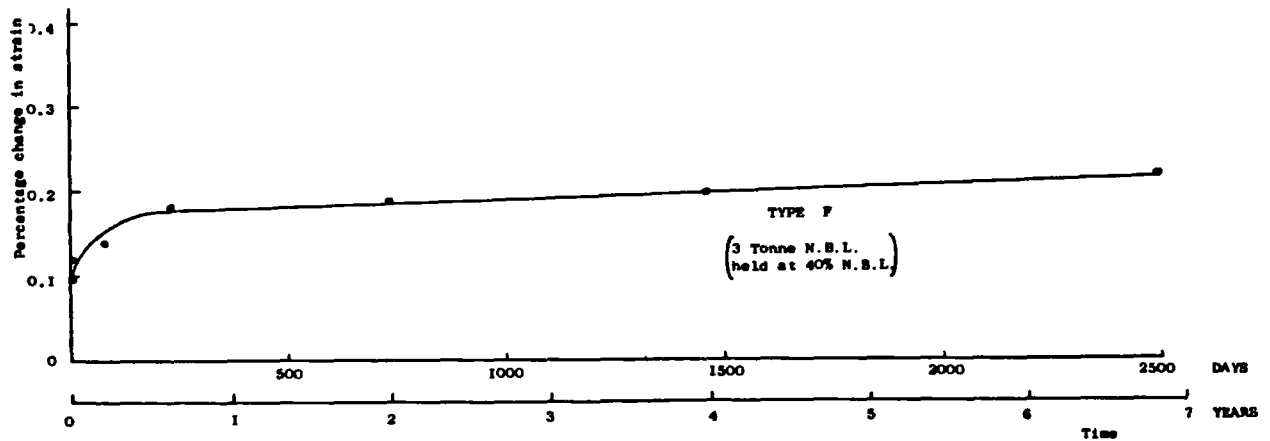


Figure 3.5 Creep curve for ICI's parallel-lay Kevlar 29 ropes (ICI trade literature).

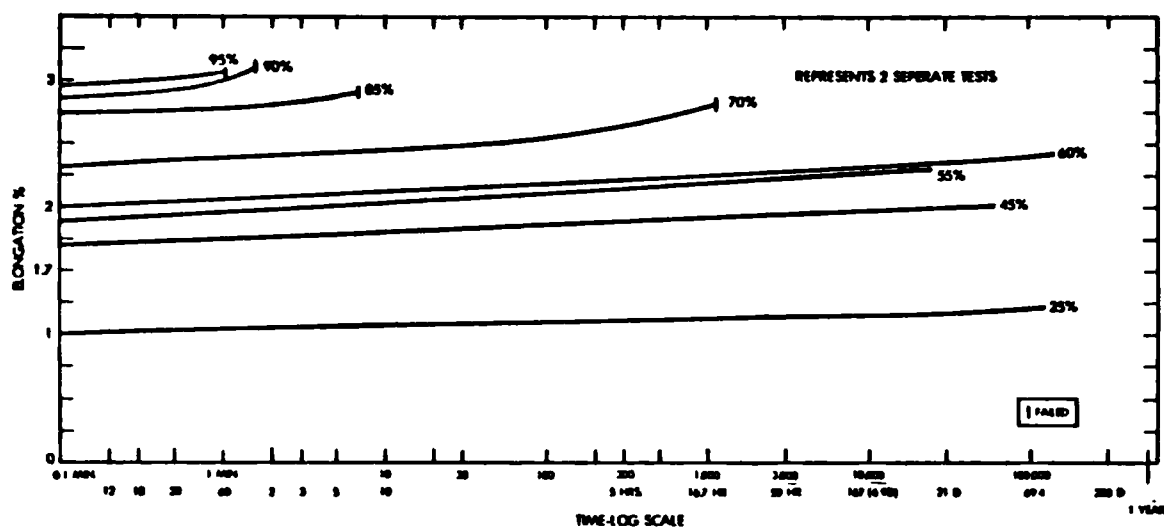


Figure 3.6 Stress-rupture and creep behaviour of a 0.25 inch diameter Uniline parallel-lay rope at various percentages of initial strength (Ferrer 1977).

3.2.4 Tension-tension fatigue

Horn et al.(1977) have shown that the good fatigue performance of Kevlar yarns is reflected well in the behaviour of ropes. This is confirmed by Ferrer and Swenson (1976) who indicate that tension-tension cycling is unlikely to affect most Kevlar ropes. Typical results (Figure 3.7) show that the behaviour of ropes made from Kevlar is superior to steel. Figure 3.8 illustrates the fatigue behaviour of ICI's parallel-lay Type F rope incorporating Kevlar 29 yarns. The curve indicates a high fatigue limit.

Wall Rope Works (USA) report a failure after 30 000 cycles for a 0.5 inch diameter Miniline rope stressed up to a maximum of 50% of the ultimate. This result appears to be lower than those obtained from Figure 3.7 and could be due to the type of rope termination employed. Indeed, Horn et al.(1977) have found that the fatigue life of ropes is often limited by the performance of terminations (3.3).

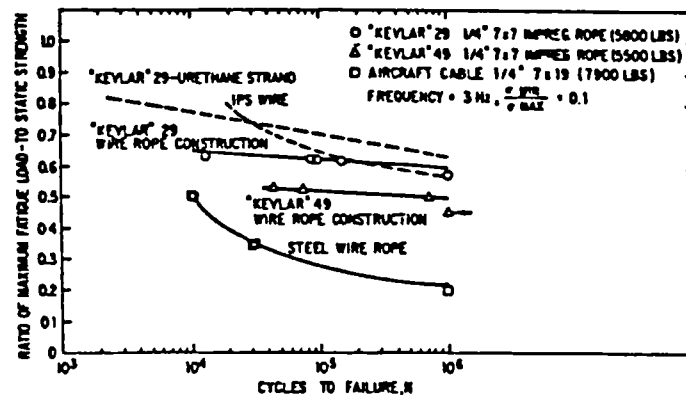


Figure 3.7 Fatigue behaviour of Kevlar and steel ropes
(Horn et al.1977).

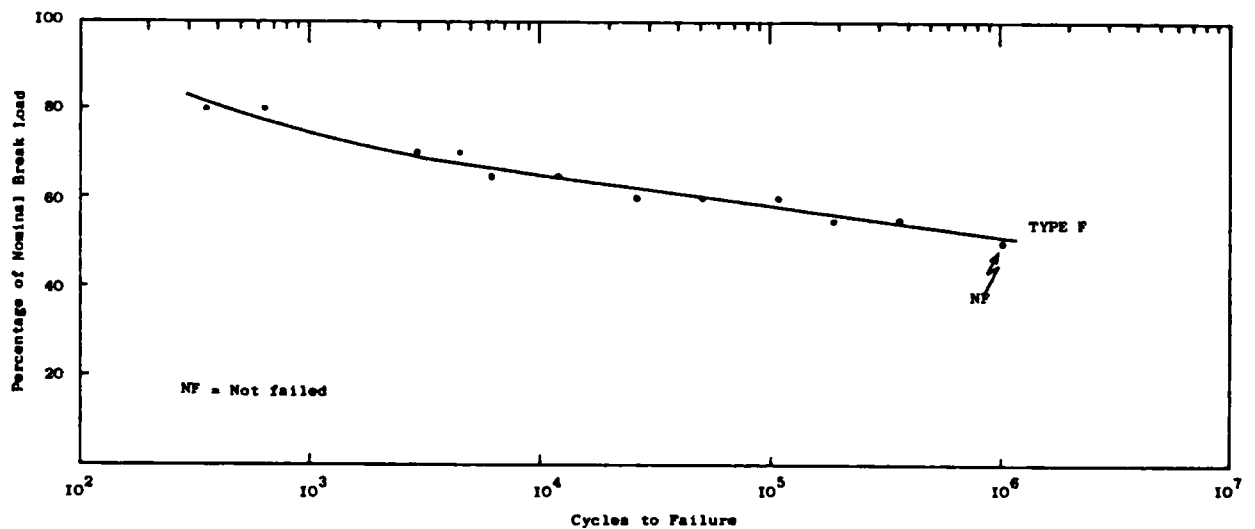


Figure 3.8 Fatigue behaviour of ICI's parallel-lay
Kevlar 29 ropes (ICI trade literature).

3.2.5 Abrasion resistance and cycling over sheaves

All Kevlar ropes are significantly affected by being bent around sheaves owing to problems of abrasion. Braided ropes are most prone to abrasion. Parallel-lay ropes perform slightly better and probably fail when pulled around sheaves of small radii because of increased tension and differential movement in the outer yarns. Twisted rope constructions of Kevlar appear to give the best performance, however, primarily because of their structure which reduces inter-yarn movement. A comparison of results is shown in Figure 3.9.

Du Pont now produce yarns with a rope finish which has improved the abrasion performance of end products considerably. In applications where abrasion resistance is critical, however, methods may be adopted which result in a performance better than for steel ropes. This involves overjacketing the individual bundles of Kevlar within a rope with nylon and using a variety of lubricating resins and oils.

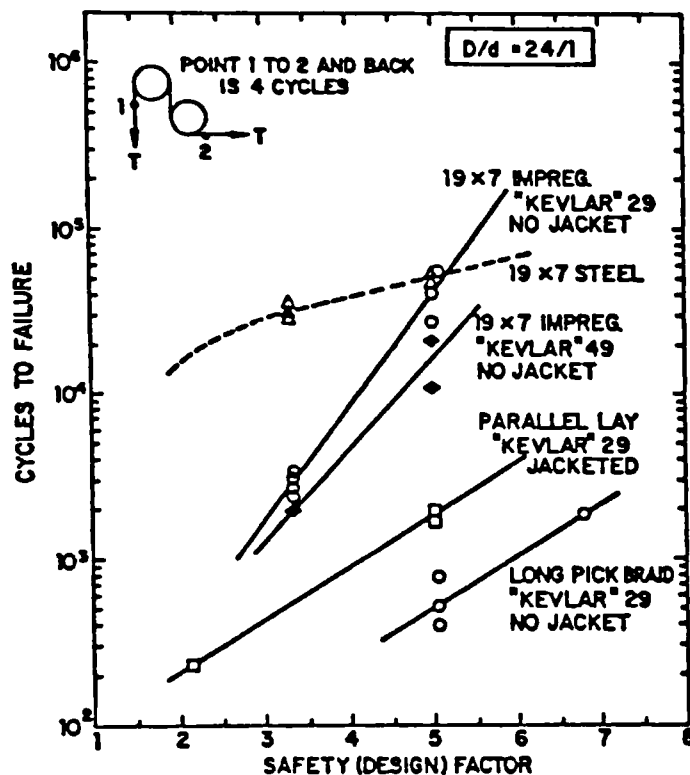


Figure 3.9 Performance of Kevlar ropes subject to reverse bending around sheaves (Horn et al.1977).

3.2.6. Environmental resistance

The environmental resistance exhibited by Kevlar yarns and fibres (2.4.10) is generally also displayed by Kevlar ropes. Some research into the specific environmental resistance of Kevlar ropes has, nevertheless been conducted.

Wilkins (1975) has investigated the behaviour of a small Kevlar 49 cable (2.6 mm diameter) when subjected to three types of environmental exposure: high pressure cycling in seawater, immersion in sea water with a high biological content under ambient pressure, and sunlight. No changes in properties were observed after the pressure cycling but significant reductions in tensile strength and modulus were measured after exposure to sunlight as would be expected from the behaviour of yarns. The tensile properties of the samples exposed to the biologically active sea water remained unchanged after 6 months exposure but deteriorated significantly after 20 months owing to the biological penetration of crustacea. In the case of the sunlight a thin jacket of black urethane was recommended to counteract the effects of ultraviolet radiation.

Bourgault (1976) and Walden (1976) both surprisingly reported substantial reductions in tensile strength after Kevlar ropes had been exposed to sea water. On the other hand, Ferer (1977) and Bourgault (1980) report only insignificant strength losses, probably due to abrasion. Bourgault (1980) explained that the earlier surprising data may have resulted from inadequate control testing, or the poor handling methods adopted on rope recovery.

More recently, however, Morgan et al. (1984) have revealed that Kevlar 49 fibres are indeed liable to hydrolytic degradation. For a fibre subjected to zero stress at 23° C and 100% relative humidity, a tensile strength degradation of 0.6% per year was predicted.

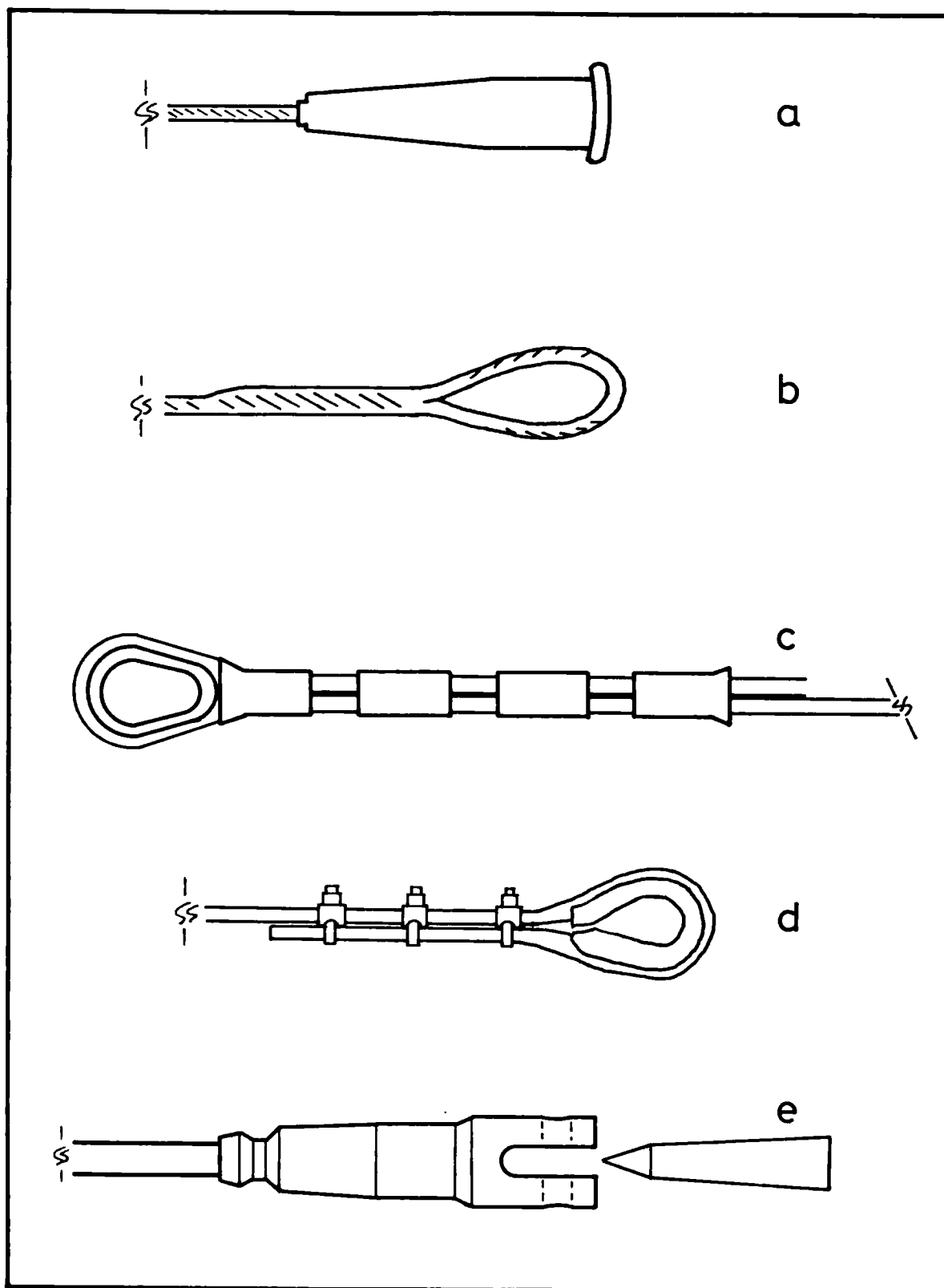
3.3 Terminations

A variety of methods exist for terminating Kevlar ropes (Figure 3.10). These include splicing, epoxy potting, or methods based on the use of wedged sockets, rope grips or pressed sleeves. The choice of termination should be influenced by the type of rope structure and the application, as well as the magnitude and nature of the loading. In general, the rope manufacturers will supply the most suitable terminations to match their respective products given the operating conditions of the rope.

Du Pont (1974), Ferer and Swenson (1976) and Horn et al.(1977) have reviewed the performance of several termination devices. All of the devices appear to behave satisfactorily under simple tensile loading as indicated in Table 3.2. Splicing and epoxy potting are commonly used for twisted and braided ropes; however, care must be taken in the choice of the splice or resin to ensure good performances under fatigue loading. If pressed sleeves or Bull Dog clips are used then it is likely that progressive abrasion will lead to premature failure under cyclic loading.

Greater care is needed when applying terminations to parallel-lay ropes, than for twisted or braided ropes, in order to ensure that the individual members are evenly stressed, since there is little member interaction in this form of construction. Even so, two successful methods for terminating parallel-lay ropes appear to have been devised. The first, developed by Wall Rope Works for their Uniline rope (now discontinued) involves a splice based on the Chinese finger-grip principle which is reported to give a reliable connection. The second, also reliable, and developed by ICI Linear Composites Ltd. for their Parafil range of ropes, is based on a tapered spike and socket device. This device is fully described in Section 3.5.4

The literature indicates that methods of splicing and epoxy potting are labour intensive. For instance, the splice devised for Uniline takes nearly one hour to accomplish (Ferer and Swenson 1976). Epoxy potting, likewise, involves a rigorous procedure of solvent cleaning, drying, resin mixing, application and curing. Indeed, Wilkins (1976) refers to this sequence of operations as extremely time consuming and



- a Reliable Electric Potting Head
- b Liverpool or Spiral Eye Splice
- c Nicopress Sleeves
- d Bull Dog Clips and Eye
- e Tapered Spike and Socket for Parafil Ropes

Figure 3.10 Typical terminations for Kevlar ropes.

expensive. Simple rope grips and sleeves, on the other hand, can be applied quickly but are suspect under fatigue loading. The device developed for Parafil ropes, however, may be applied in a matter of minutes, for typical rope sizes, and appears to behave well under a variety of loading conditions.

Termination	% of Rated Strength	
	Steel	KEVLAR
Hand Splice	80-90	80-100
Wedge Sockets	70-90	80-100
Epoxy Sockets	-	80-100
Pressed Sleeves	90-95	80-100
Swaged Sockets	100	80-100

Table 3.2 Behaviour of termination devices under tensile loading (Du Pont 1974).

Doubts regarding the integrity of Kevlar rope terminations still exist. Ferer and Swenson (1976), however, conclude that appropriately chosen rope fittings perform very well. These investigators also refute the claim that Kevlar ropes are difficult to terminate.

3.4 Applications

3.4.1 Introduction

Kevlar ropes are replacing steel cables where high tensile strength, low weight, strong resistance to corrosion, good fatigue and creep performances, and toughness are important. The majority of applications have been offshore either as mooring lines, oil rig riser tensioner lines or pendant lines. Onshore they have found general industrial use and have been used as guys for masts and for general support systems. They have also found considerable use as the load-bearing element of electromechanical cables. The scope for future uses is unlimited but two likely possibilities are as cables

for suspension bridges and as tendons in prestressed concrete.

3.4.2 Mooring lines

A recent application described by O'Hear (1983) has been the adoption of 100 tonne braided lines, manufactured by Seine and Lys of France, for use as tanker mooring lines at the Saudi Arabian Oil Terminal, Ras Tanura. The port authority set a 3% elongation limit at 55% of the rope breaking strength so as to avoid the possibility of the failure of oil supply lines due to drifting. Kevlar ropes were adopted owing to their strength, stiffness, flexibility and resistance to corrosion.

Cables of Kevlar 29, produced by Vermeire of Belgium (see Appendix 1) have been used extensively for mooring applications during construction of the Eastern Scheldt storm barrier in Holland. They were chosen mainly because of their large specific-strength advantage when compared with steel, their corrosion resistance, their stiffness and their ease of handling.

A potential use for Kevlar cables, considered by Salama (1984) is as the mooring lines for deepwater tension leg platforms. Steel mooring lines require complicated tensioning, handling and flotation systems. Steel tubulars, which have recently been employed on Conoco's Hutton tension leg platform in the North Sea are clearly a viable alternative, but these require regular inspection to detect corrosion, particularly at joints. The self-weight of steel tubulars is a further disadvantage; it has added significantly to the cost of the platform in terms of buoyancy. ICI's parallel-lay Parafil ropes, however, may provide a feasible and more economic solution; they would also be maintenance-free. Salama (1984) recommends their use for lightweight platforms in intermediate water depths.

3.4.3 Riser tensioner lines

Riser tensioner lines are used to connect semi-submersible oil rigs to fixed drilling tubes (risers) on the sea bed. Du Pont have published trade literature outlining the advantages of using Kevlar

ropes as riser tensioner lines. The main benefit arises from their excellent fatigue performance which increases the replacement period, thus giving considerable economic savings. Their ease of installation and good corrosion resistance are further attractive qualities in this field. As a result, Kevlar ropes have been adopted as riser tensioner lines off the east coast of USA and in the Gulf of Mexico.

3.4.4 Pendant lines

The advantages of using Kevlar ropes as pendant lines (anchor lines) have been demonstrated in water depths ranging from 200 feet to 1200 feet throughout the world. Their lightness compared to steel lines results in either reduced buoy sizes or extended rig depth capability. The reduction in handling time can also result in worthwhile economic benefits.

3.4.5 Onshore uses

Kevlar ropes have shown advantages in many industrial applications where hoisting, towing or winching is required. Mining cables and crane ropes are typical applications. Also, owing to their good dielectric properties they have successfully been adopted for antennae guys and for tramway cable support systems. In the pleasure field they are frequently used for mountaineering, pot-holing and for boatrigging. Ropes made from Kevlar have also been employed in the aerospace industry as tethers for meteorological balloons, helicopter hoists and glider tow ropes. The good tensile properties, environmental stability, low weight, good fatigue resistance and ease of handling possessed by Kevlar ropes give rise to an unlimited scope of future applications.

3.4.6 Electromechanical cables

Traditionally, the loadbearing elements of electromechanical cables have been of steel. The constraints imposed by the high self-weight of steel, however, have led designers to consider the use of lighter

reinforcing materials. In the past, a reduction in cable self-weight by the replacement of steel with fibres such as nylon or polyester was not practicable due to their low moduli which would have led to loading of the conductor and eventual failure. When Kevlar became available, however, the beneficial combination of high specific strength and stiffness was soon recognised; indeed, the use of Kevlar in electromechanical cables is now a major application of the material.

Holler et al.(1975), Wilkins (1975) and Wilkins et al.(1976) have all considered Kevlar-based electromechanical cables. Holler et al.(1975) have found them to be superior to steel cables not only mechanically but also electrically. A typical cross-section through a Kevlar-based electromechanical cable is shown in Figure 3.11. They have now been readily adopted as data and sonabuoy mooring cables, air and sea towed antennae, and as cables for deep ocean systems and fibre optics.

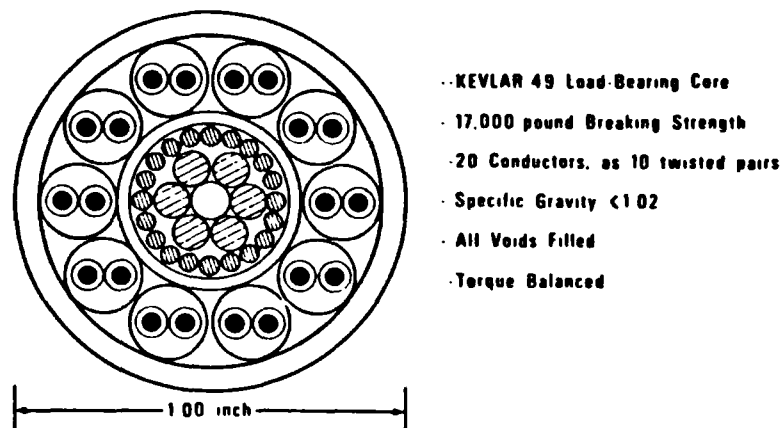


Figure 3.11 Typical cross-section through a Kevlar-based electromechanical cable (Wilkins 1975).

3.4.7. Future applications

An exciting proposal for ICI's Parafil ropes was to use them as the cables for a suspension bridge across the English Channel. The scheme, entitled Eurobridge, was given serious consideration as one of several competing proposals for a fixed link between England and France. The Eurobridge scheme encompassed seven spans of five kilometres each, carrying a twelve lane motorway on four levels as shown in Figure 3.12. The five kilometre spans, made feasible because of the excellent specific strength of Parafil (2.4.2) would have been three and a half times longer than the span of the Humber Bridge, which is currently the longest span in the world. Undeterred by the decision to build a tunnel rather than a bridge the Eurobridge consortia has remained intact and is now putting forward a serious bid to build a Parafil bridge across the River Thames at Dartford (New Civil Engineer 1986).

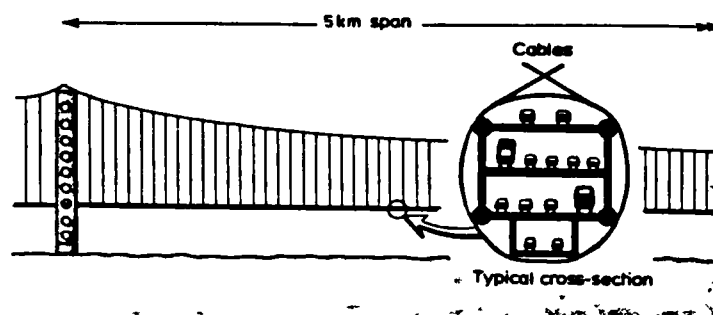


Figure 3.12 The Eurobridge scheme for a fixed link across the English Channel (Hamer 1985).

Another likely application for Kevlar, and the one which is the subject of the bulk of this thesis, is as tendons in prestressed concrete. Parallel-lay ropes such as ICI's Parafil range are the most suitable for this application (3.5).

3.5 Parafil ropes and their suitability as prestressing tendons

3.5.1 General description

Parafil (derived from PARAllel FILaments) is a generic trade name for the parallel-lay ropes produced by ICI Linear Composites Limited. A Parafil rope consists of a closely packed core of high strength continuous synthetic yarns encased in a durable polymeric sheath. The yarns contain a nominal twist but the core itself may essentially be regarded as twist-free.

Parafil ropes are light in weight, maintenance-free and have adequate strength and stiffness for structural applications to be considered. At present, ropes with breaking loads ranging from 0.2 to 1500 tonnes are manufactured and larger ropes are being developed. They are manufactured in three grades containing either Terylene (polyester) or Kevlar yarns as indicated in Table 3.3. Typical load-extension curves, contained in ICI trade literature are shown in Figure 3.13.

Material	Tensile strength (MPa)	Elastic modulus (GPa)	Specific gravity
Parafil Type A (Terylene)	617	12	0.9
Parafil Type F (Kevlar 29)	1926	78	1.0
Parafil Type G (Kevlar 49)	1926	127	1.0
Steel wire	1670	200	7.8
Steel strand	1750	200	7.8
Alloy bar	1030	165	7.8

Table 3.3 Basic properties of parafil and steel products.

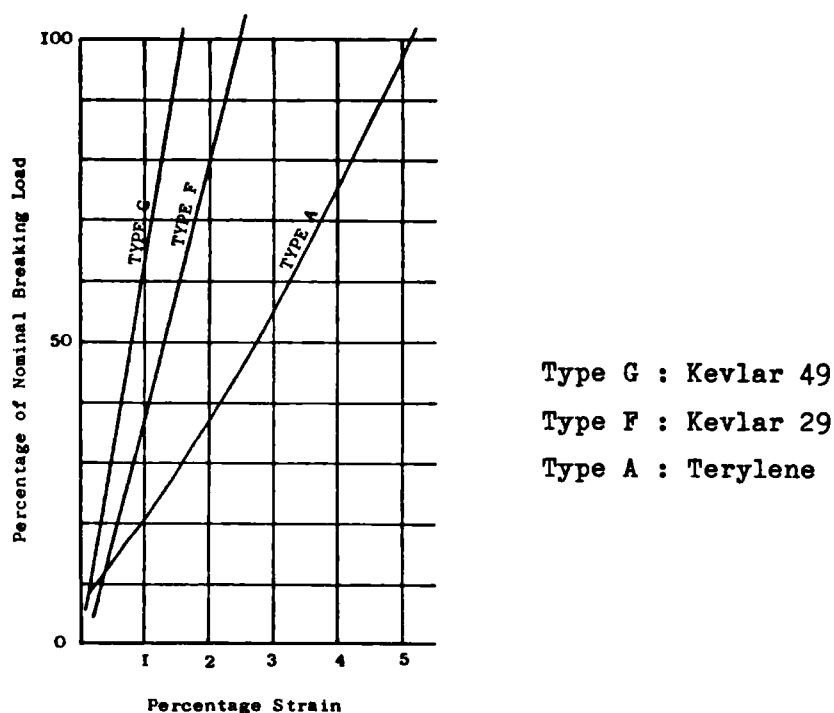


Figure 3.13 Load-extension curves for ICI's Parafil ropes (ICI trade literature).

3.5.2 Requirements for a prestressing tendon

The main requirements for a prestressing tendon are:

- (1) high tensile strength
- (2) high stiffness
- (3) low creep (stress-relaxation)
- (4) good corrosion resistance, and
- (5) good fatigue performance.

Ferer and Swenson (1976) and O'Hear (1983) have indicated that Kevlar ropes of parallel-lay construction display the highest strength efficiency factors (ratio of rope strength to that based on multiples of yarn strength). Parafil ropes based on Kevlar, therefore, possess the appropriate structure to satisfy the first requirement. Indeed, the tensile strength of the ropes exceeds that of traditional steel products.

Tendons of high stiffness are required so as to minimise jack extensions and to allow the adoption of standard stressing equipment. Twisted and braided ropes give rise to large amounts of

constructional stretch (Figure 3.3) and would be unsuitable for prestressing. Parallel-lay ropes based on the stiffer Kevlar 49 yarns, on the other hand, display minimal constructional elongation and offer the best stiffness that can be achieved. Their stiffness approaches that of steel.

Prestressing tendons with low creep characteristics are necessary so that a high proportion of the initially applied load is maintained throughout the life of the structure. Horn et al.(1977) have indicated (Figure 3.4) that a parallel-lay rope of Kevlar 49 is the preferred material for a creep-critical situation.

Good corrosion resistance is needed to ensure structural integrity. Steel tendons are liable to corrode if they come into contact with water, salts or aggressive chemicals. For this reason, steel tendons are usually placed within internal ducts, although this is not structurally necessary. Furthermore, precautions such as grouting or the application of a corrosion protection material are usually taken. Even so, there have been instances of tendon corrosion which have resulted in costly replacement or sometimes structural collapse (e.g. Berlin Kongresshalle). On the other hand, the chemical resistance of Kevlar is excellent (2.4.10) and consequently, Parafil tendons may essentially be regarded as non-corrodible. Kevlar does, however, lose strength after prolonged exposure to highly alkaline solutions and so the high alkalinity associated with the hydrated cement paste of concrete may give rise to similar deterioration. However, the outer thermoplastic sheath of Parafil ropes would preclude this possibility.

Prestressing tendons are subject to small fluctuations in stress owing to transient loading or temperature effects. An acceptable fatigue performance is therefore a prerequisite. The fatigue characteristics of parallel-lay ropes are superior to steel (Figure 3.8). While twisted and braided ropes perform even better, fatigue is unlikely to be a problem in Parafil ropes when used in prestressed concrete.

3.5.3 Parafil Type G ropes

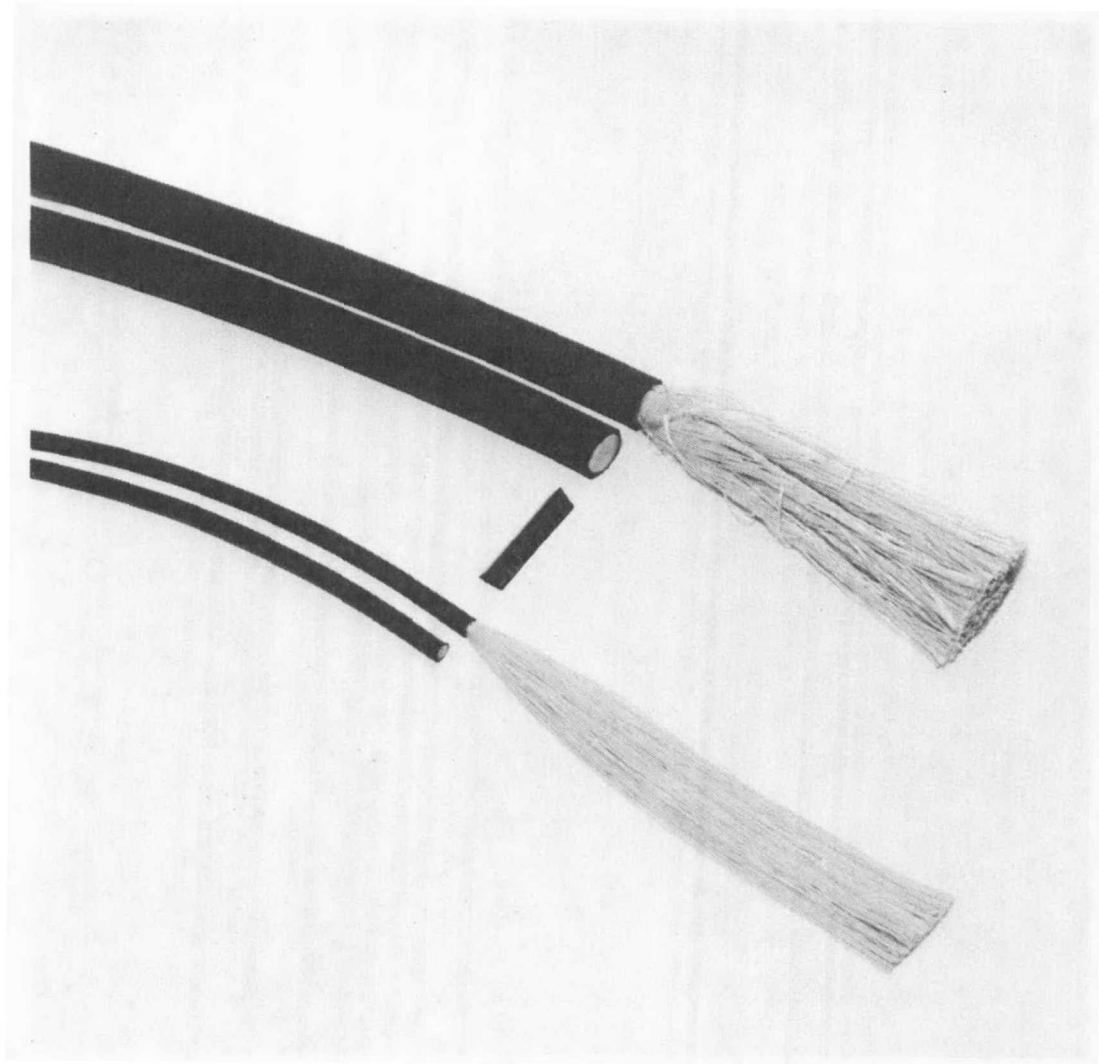
It can now be concluded that Parafil Type G ropes based on Kevlar 49 are the most suitable for satisfying the criteria for a prestressing tendon. The research is consequently based on these ropes.

Figure 3.14 shows a 6 tonne and a 60 tonne Type G Parafil rope used in the experimental study; these ropes have overall diameters of 11 mm and 31 mm respectively (for further details of all Kevlar-based Parafil ropes see Appendix 1). Type G ropes consist of yarns of 2370 decitex (one decitex equals the weight in grams of 10 000 metres of material) with a nominal diameter of 0.65 mm. In turn, each yarn consists of 1000 individual Kevlar 49 filaments of 0.01294 mm diameter. The minimum yarn breaking strength quoted by Du Pont (1980) is 38.5 kgf but ICI obtain results consistently higher than this (Chapter 8).

Wilfong and Zimmerman (1977) have investigated the effect of twist on the tensile strength of Kevlar yarns and their results are shown in Figure 3.15. The applied twist in commercially available Kevlar yarns is equivalent to a twist multiplier factor of 1.1 which is optimal for tensile strength. While Wilfong and Zimmerman (1977) do not state the grade of the Kevlar tested in their work on twist, Ferer and Swenson (1976) suggest that the incorporation of twist does not improve the strength of Kevlar 49 yarns owing to their higher modulus which prevents them from accommodating the random slack generated.

Du Pont (1980) recommend a process tension of at least 0.05 grams/denier when fabricating ropes so as to achieve high strength with low scatter. For a 2130 denier yarn (i.e. 2370 decitex) this represents a load of 0.1 kgf which is less than 0.3% of the yarn breaking strength. It is possible that ICI have adopted a similar tension in their production process.

The outer sheaths of Parafil ropes are usually made of black polyethylene. Other sheathing materials are available depending on the application. The sheath not only holds the rope together but provides protection against ultraviolet radiation and external abrasion.



scale marker 50 mm

Figure 3.14 6 tonne and 60 tonne Type G Parafil Ropes.

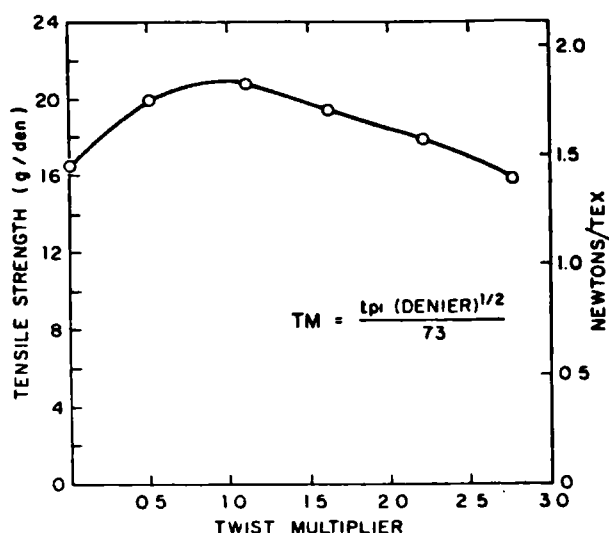


Figure 3.15 The effect of twist on the tensile strength of Kevlar yarns (Wilfong and Zimmerman 1977).

3.5.4 Parafil terminations

Terminations for Parafil ropes based on a conical wedge principle have been developed and patented by ICI. They are usually made from anodised aluminium alloy or galvanised mild steel. However, stainless steel or other materials may be used to suit particular requirements.

Figure 3.16 shows the terminal hardware for 6 and 60 tonne Type G ropes. The standard design for the 60 tonne terminal was modified slightly for the tests described in this thesis to facilitate prestressing by increasing the overall length and incorporating an inner thread (Figure 3.17). Commercially available terminals, developed primarily for external applications, are tapered and contain front and back weather-seals.

The fitting of end connectors to Parafil ropes is a straightforward operation as illustrated in Figure 3.18. Firstly, the rope is threaded through the terminal and a length of sheath, equal to the spike length, is carefully removed. An even annulus of parallel yarns is then arranged vertically around the end of the terminal. Following this exercise, the spike is pushed vertically downwards while simultaneously pulling on the rope. Finally, the spike is secured with a slight blow from a hammer whilst maintaining tension in the rope.

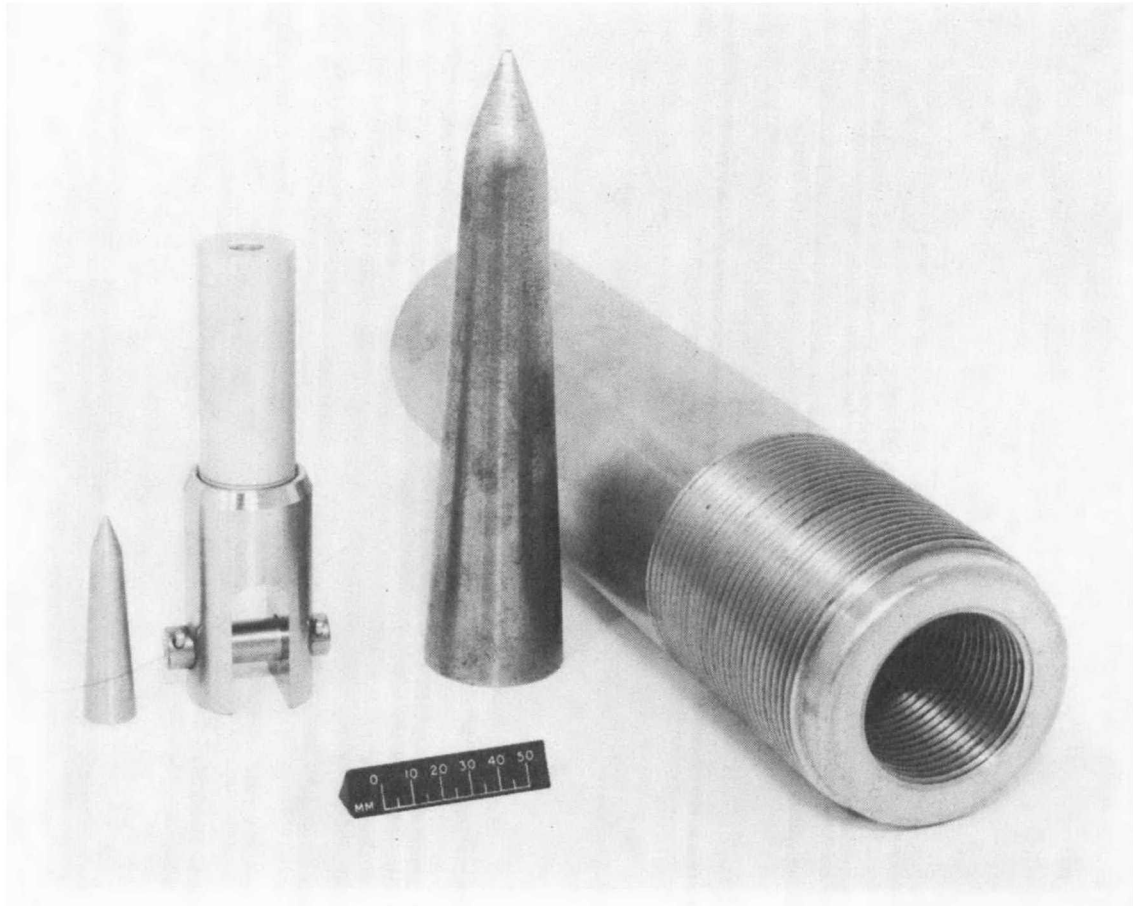


Figure 3.16 **Terminals for 6 tonne and 60 tonne Type G**
Parafil Ropes.

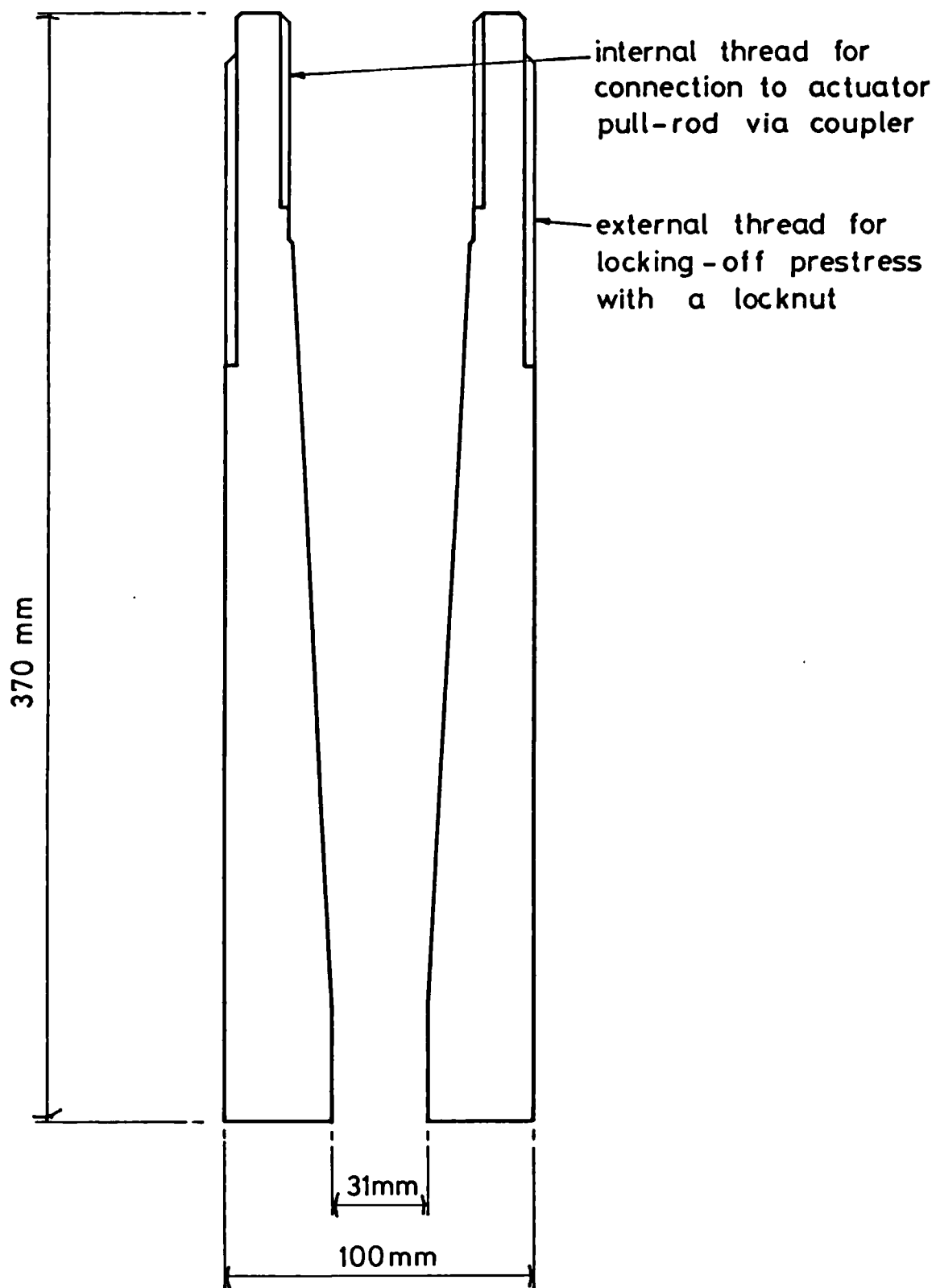


Figure 3.17 Longitudinal section through a 60 tonne Type G terminal modified for prestressing.

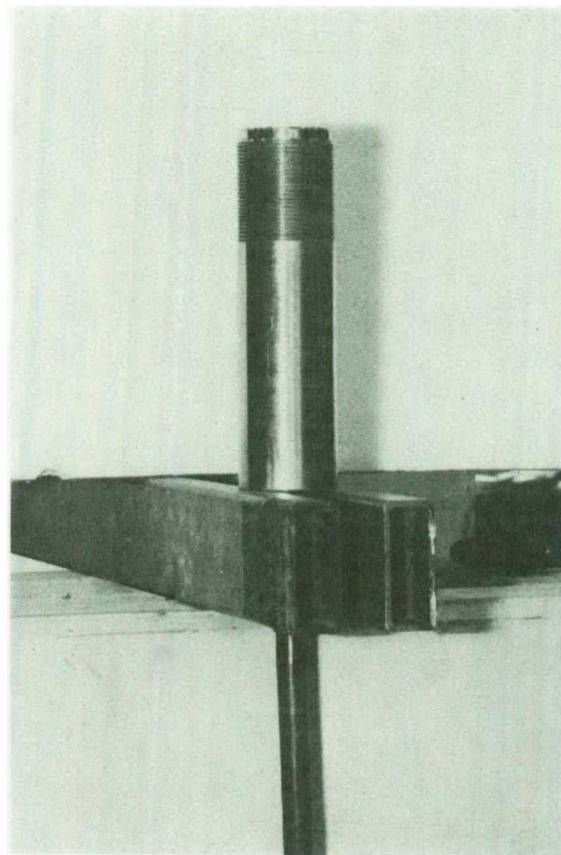
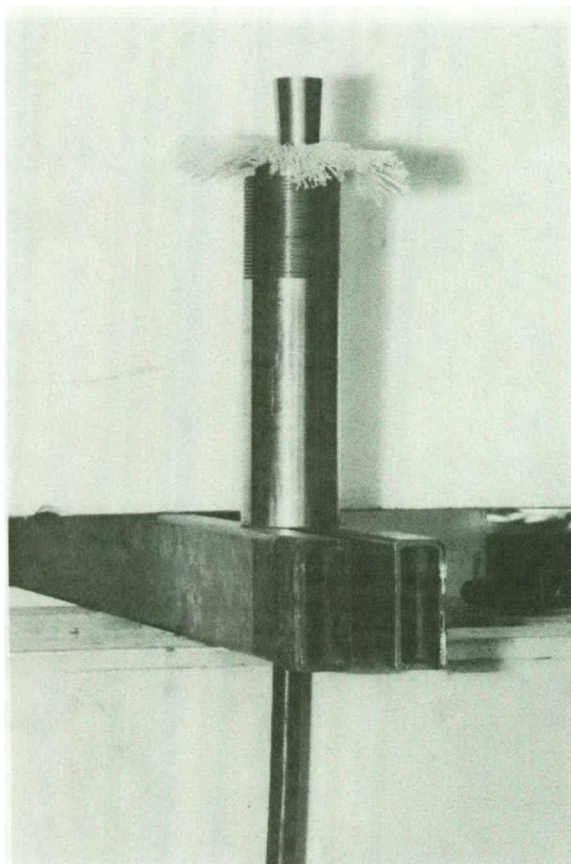
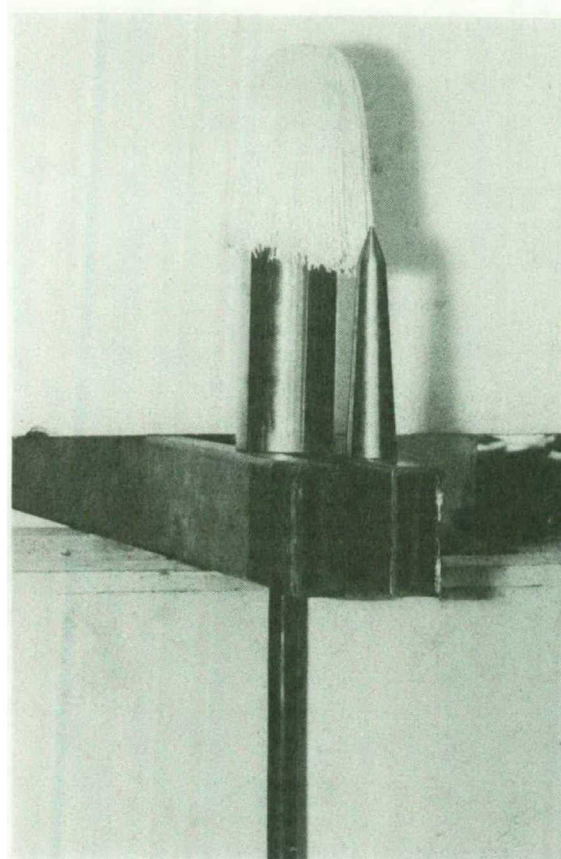
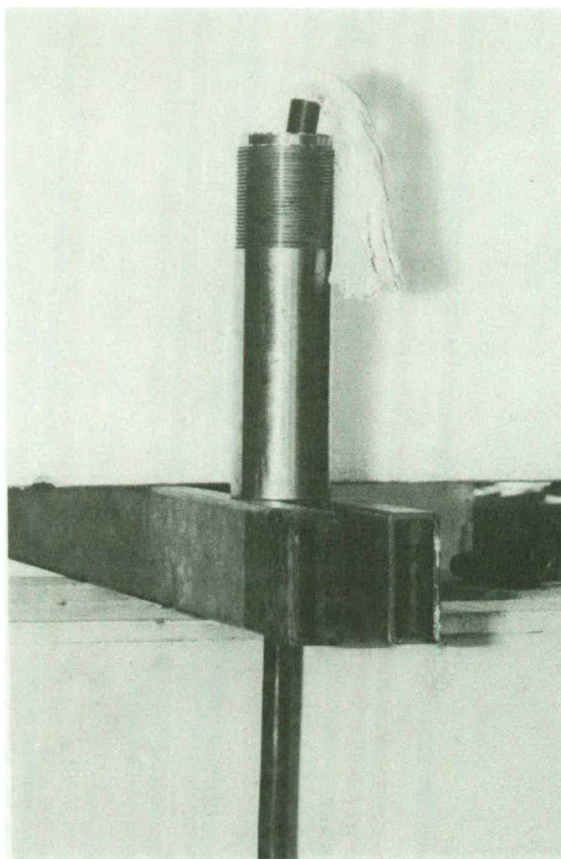


Figure 3.18 Method of fitting Parafil rope terminals.

3.5.5 The relevance of earlier work to prestressing

Although some general properties of parallel-lay ropes have been assessed (e.g. Ferer and Swenson 1976, Horn et al. 1977) there is clearly considerable danger in accepting these assessments as a sound basis for the design of prestressed concrete members incorporating Parafil Type G ropes. Not only is there insufficient information available (particularly regarding stress-rupture, stress-relaxation and material variability) to enable prestressed concrete members to be designed but there is a large degree of uncertainty regarding the generality of the results. For instance, the properties of parallel-lay ropes are likely to depend on: the precise rope structure, the rope size, the fabrication process, the type of terminations employed and the method of testing. As part of the process to establish reliable design guidelines, an experimental investigation of pertinent Type G rope properties in particular, was deemed, therefore, to be of paramount importance. Nevertheless, despite the uncertainty and incomplete information, the earlier work of other investigators has helped to identify prestressing as a potential application for Parafil Type G ropes.

3.6 Alternative materials and their suitability as prestressing tendons

3.6.1 Introduction

Potential competitors to Kevlar as a new material for prestressing include glass, carbon and boron. The fabrication of ropes (with a suitable structure) based on these alternatives is feasible since it is now possible to produce continuous filaments from all three materials. Glass, carbon and boron have strengths similar to or greater than Kevlar and so superficially appear suitable for prestressing. The potential competitors are also resistant to most chemicals but, as with Kevlar, their behaviour at elevated temperatures is inferior to steel. Some comparative properties are shown in Table 3.4.

Material	Tensile Strength (MPa)	Modulus (GPa)	Approx cost (£/kg)	Cost/tonne force/metre (pence)*
Kevlar 49	2760	124	22	11.3
E-Glass	2500	70	1.2	1.2
Carbon (stiff)	1800	410	30	28.6
Carbon (strong)	2800	270	30	18.4
Boron	3150	379	200 (1980)	155
Prestressing Steel	1750	200	0.6	2.6

* Based on tensile strength, not working stress.

Table 3.4 Comparative properties of competitive materials.

3.6.2 Glass

There are five predominant glass compositions used in the production of continuous-filament glass-fibre products. S-glass with a higher tensile strength and modulus than the more readily available E-glass appears to be the most suitable for structural applications. S-glass, however, is less resistant than E-glass to strongly alkaline solutions and so this may preclude its use in prestressed concrete. Furthermore, S-glass is relatively costly to make, having been developed for the sophisticated aerospace market.

Whilst their average tensile strengths are high, glasses are very notch-sensitive. This tends to cause considerable variability in the tensile strength of the material which limits actual design strengths. Notch-sensitivity also has serious implications in terms of fatigue performance.

The main problem with glass fibres, however, is their poor stress-rupture behaviour which is exacerbated by wet conditions. Operating stresses for conventional glass fibres would have to be restricted to about 10% of their short-term strength to prevent failure from static fatigue. Optical fibres, with high tensile strengths, have a special coating which reduces the stress-ageing problem but it is not believed that these coatings could be applied economically on the scale required for prestressing.

The relatively low moduli of glasses would give rise to large jack extensions during prestressing but this may be offset against some reduced prestress losses. The behaviour of glasses at elevated temperatures, however, is questionable; they retain about 25% of their initial strength at 500°C.

Despite the practical disadvantages of using glass fibre for prestressing, resin bonded glass fibre bars have been used as tendons for a prestressed concrete footbridge in Dusseldorf, West Germany (Weiser 1983). The bridge of 6.55 metres span incorporated resin bonded E-glass bars with a glass volume content of 70%, manufactured by Bayer. The bars had a tensile strength of 1600 MPa and an elastic modulus of 52 GPa. Surprisingly this type of material has also displayed satisfactory stress-rupture behaviour, sustaining more than 70% of the initial breaking load for one year (Rehm and Franke 1979).

The footbridge application indicates that solutions to the problems associated with glass fibres can be found. Glass fibres are also the cheapest high strength fibres available, by far. This alone should be a sufficient spur to encourage further research into possible structural applications.

3.6.3 Carbon

The production of carbon fibres in the United Kingdom has successfully been derived from the polymer precursor polyacrylonitrile (PAN). PAN has remained the main precursor with pitch replacing rayon as an alternative.

The development of carbon fibres has progressed significantly over the last fifteen years. By changing the processing temperature or possibly the precursor chemistry it is now possible to produce a range of carbon fibres with varying but reliable properties.

Carbon fibres which fall into the 'high-strength' category would be the most appropriate for prestressing. These have strengths similar to those of Kevlar 49 filaments and a stiffness similar to that for steel tendons. This would mean that carbon-fibre based tendons would be compatible with standard stressing equipment.

Carbon fibres are less prone to static fatigue than Kevlar or glass fibres (Chiao et al.1977a) and in composite form can typically sustain 85% of their initial breaking load for 10 years. The fibres are, however, susceptible to oxidation at temperatures above 400 °C. Furthermore, the development of suitable end terminations for prestressing may prove problematic.

The main disadvantage with carbon fibres used to be their cost but this has lowered considerably in recent years to £30 per kilogramme. If the improved stress-rupture properties of carbon fibres were shown to hold for ropes then it is possible that their cost in terms of working stress would approach that of Kevlar 49.

3.6.4 Boron

Boron was the first available lightweight high performance fibre, yet the feasibility of large scale production still remains doubtful. The very limited worldwide production of boron is reserved primarily for key defence contracts and is difficult to obtain commercially. A small amount of boron fibres is procured by foreign manufacturers of high class sporting equipment (tennis racquets etc) but the volume content of boron is probably of the order of 3%.

With the cost of boron in 1980 at two hundred dollars per kilogramme (and now believed to be substantially higher) it is unlikely that boron will find an application in prestressing. If the difficulties of production were to be overcome then boron would be a candidate in terms of tensile strength. The high stiffness of boron (379 GPa),

however, would have serious implications in terms of prestress losses and the accuracy with which prestressing loads could be applied. Furthermore, similar to carbon fibre, boron is susceptible to oxidation at elevated temperatures.

CHAPTER 4: TENSION TESTING OF 60 TONNE TYPE G PARAFIL ROPES

4.1 Introduction

Ultimate strength and modulus of elasticity are the most important short-term properties of any structural material. An evaluation of their magnitude permits an immediate preliminary assessment to be made of the suitability of a material for a particular application.

ICI Linear Composites Ltd. quote values in trade literature (Appendix 1) for the moduli of elasticity for Parafil ropes and also for the strength of Parafil ropes in terms of nominal breaking load. The cross-sectional area of core yarn is directly proportional to the quoted nominal breaking load throughout the range which may imply that size effects are negligible. Alternatively, since the quoted nominal breaking loads are believed to be the minimum that can be expected it is simply possible that the smaller ropes display more tensile strength 'over-capacity' than the larger ropes. For whichever reason, these values do not form a rational basis for a theoretical study.

A preliminary study of the ultimate strength behaviour of Parafil Type G ropes was thought essential, therefore, since it would:

- (1) provide good initial experience of handling the material
- (2) enable an independent verification of the short-term tensile properties to be made
- (3) permit the mode of failure to be observed which often has structural implications
- (4) provide an indication of the variability in material strength which will influence engineering design strengths
- (5) form the basis for investigations into other properties.

This chapter, therefore, gives the details of a study of the behaviour of Parafil Type G ropes when subjected to monotonically increasing loads. The test rig, the method of stressing, the data acquisition system, and the instrumentation are first described in detail; these topics are common to much of the later experimental work and reference will be made to the relevant sections in some of the chapters which follow. The chapter continues with a description of the test

procedure, the reporting of the results, and a discussion of material behaviour.

Sixty tonne Type G Parafil ropes were chosen for this study since they were the smallest ropes that would be practicable as prestressing tendons. Furthermore, owing to the large number of yarns in a 60 tonne rope and the statistical properties of large bundles (Chapter 8) it is probable that when consideration is given to other Parafil ropes of practical application in the field of prestressing the effects of rope size may be ignored.

4.2 Test rig

The test rig is shown in Figure 4.1 and has a capacity for testing ropes of 8 metres length up to 100 tonnes strength. All ropes were tested within a steel box-section which not only acted as a stiff and structurally efficient reaction-frame but provided a high degree of safety to the test area.

The length of the rig was largely determined by the desire to test the maximum possible length of rope within the limit of the stroke of the actuator. The resulting choice of 8 metres was deemed sufficient to eliminate end-effects and also proved fairly economic in terms of commercially available stock lengths of steel box-sections.

The box-section employed was a mild steel (grade 43c) square hollow rolled section of dimensions 250 mm x 250 mm x 12.5 mm thickness. This size was able to accommodate all stressing components with ease and also satisfied strength and slenderness criteria when checked in accordance with the British Standard for structural steelwork (BS 449:Part 2:1969).

The box-section was restrained against longitudinal movement at one end (see Figure 4.2) but was free to slide on PTFE bearings at the stressing end, thus minimising friction. The restraint was necessary since it was important to confine the elastic movement of the box to one end of the rig when taking displacement readings.

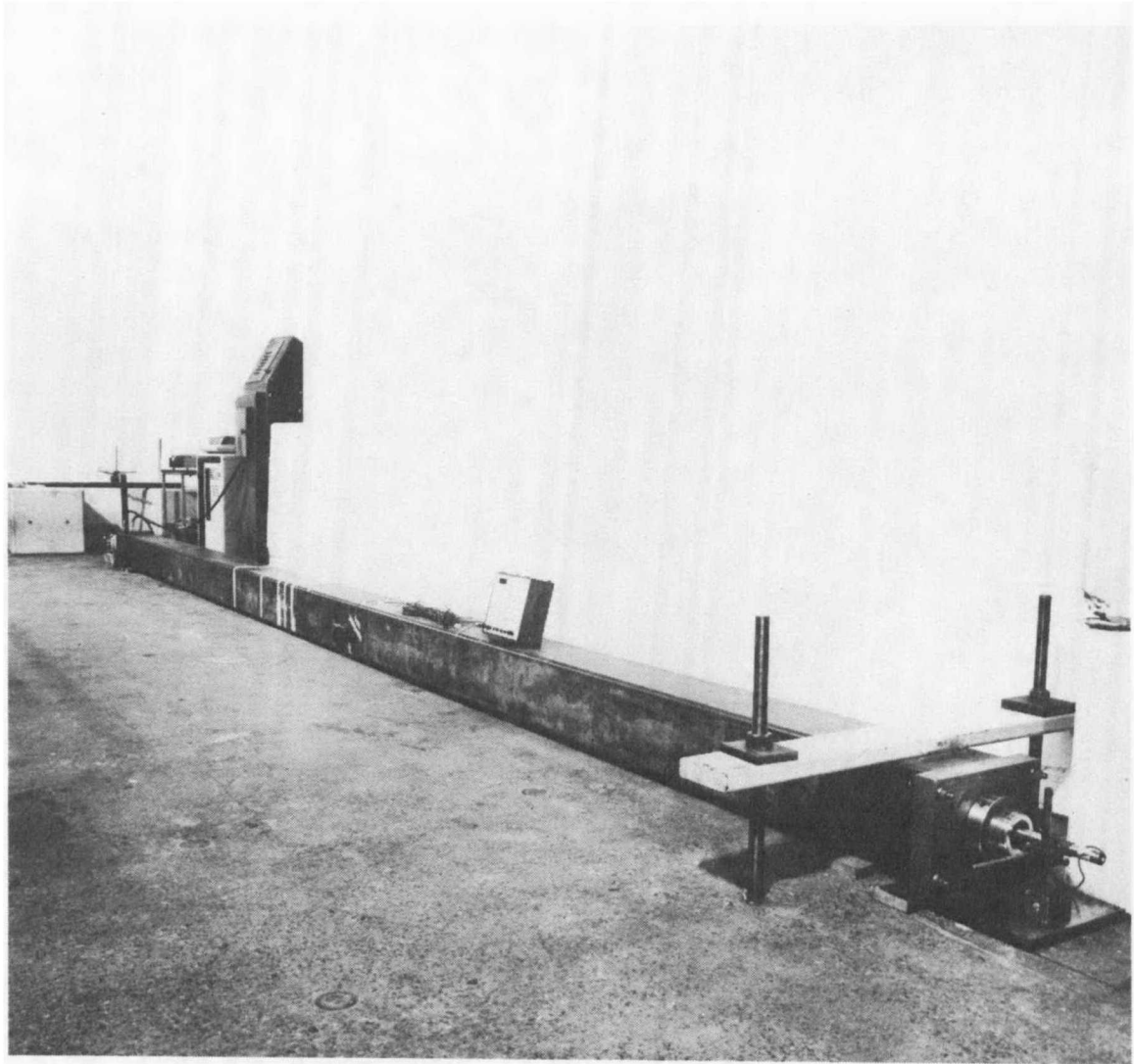


Figure 4.1 General view of test rig.

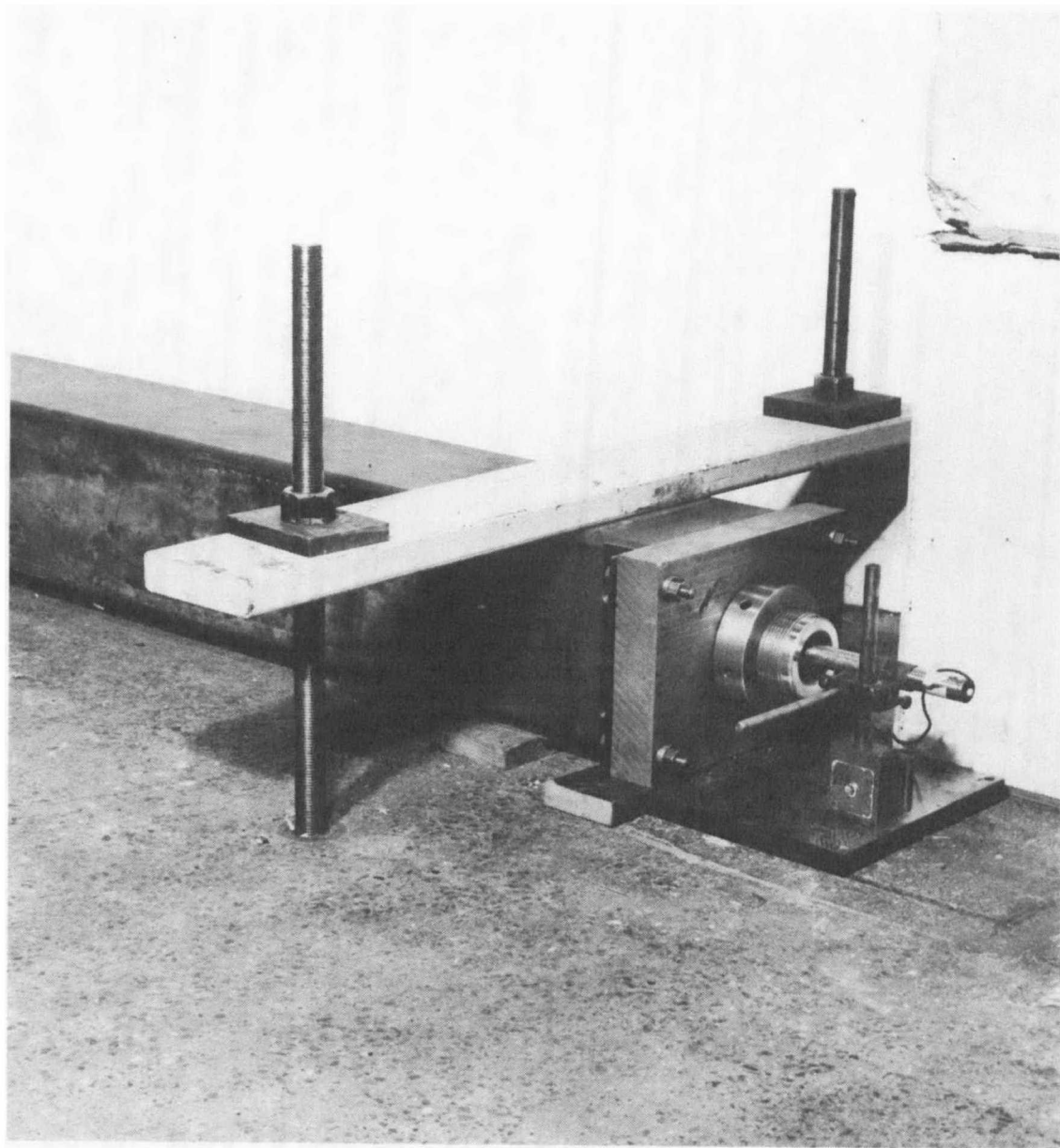


Figure 4.2 View of dead-end anchorage and LVDT measuring spike bed-down.

The rig was designed to be as adaptable as possible and was not constructed solely for tension tests. Three types of test may be performed:

- (1) tension tests - straightforward 'pull' tests at any desired stressing rate
- (2) stress-relaxation tests - tests in which the rope is loaded to the required level, 'locked-off', and allowed to relax at constant strain (Chapter 5)
- (3) stress-rupture tests - tests in which the load is held constant and the time required to produce a creep-failure is measured (Chapters 6 and 7).

4.3 Method of stressing

A stressing system had to be designed for Parafil ropes which would be suitable not only for the planned series of tests but also for prestressed concrete applications. In any prestressing system there must be facilities for:

- (1) applying the load accurately and in a controlled manner
- (2) 'locking' the load permanently into the concrete member
- (3) removing the stressing equipment.

Upon these requirements should be imposed the constraint of size; the permanent anchorage should not be so large as to influence the overall concrete member size or to cause problems in the design of end-blocks.

Commercially produced Parafil rope terminals rely on fork or eye fittings for the attachment of the rope to some stressing or tensioning device. For many applications (e.g. rigging and mooring) the tensioning device is a simple 'bottle-screw'; this would be totally unsuitable for prestressing and testing owing to:

- (1) the need to leave the device in-situ
- (2) problems of access in applying the load
- (3) the magnitude of the loads involved
- (4) the need to have adequate control during tests.

The necessity of applying the loads hydraulically was, therefore, clearly unquestionable. Even so, while it would have been a straightforward matter to use a hydraulic jack in conjunction with the

commercial fork and eye fittings, and perform certain types of test, the arrangement would still not have satisfied the requirements for prestressing since it does not include a facility for permanently anchoring the load.

The design eventually selected is illustrated in Figure 4.3. The secondary fork or eye fittings were discarded; the existing thread on the main terminal body was then used to accommodate a locknut which could be tightened against an end-plate to lock the load permanently into the box-section (stress-relaxation tests only) or prestressed concrete member. A voided steel section, abutting the box-section (or concrete member) allowed access to the external thread of the terminal to be gained when required.

The terminal body was extended slightly to allow an inner thread of sufficient length to be incorporated; the inner thread enabled a connection to be made between the terminal and the pull-rod of a hydraulic jack, via a coupler (see Figure 4.3). This thread (as well as those of all new components) was designed in accordance with BS 3580: "Guide to design considerations on the strength of screw threads". Owing to the size constraints that need to be considered in prestressing applications the coupler itself, was manufactured from a steel of high yield strength such that its diameter was smaller than that of the terminal body.

The pull-rod, inserted through the centre-hole of the actuator, was a 50 mm diameter Macalloy bar. The bar measured 1700 mm in length and was fully threaded. Its characteristic failing load was 1960 kN giving a safety factor of about 2 in terms of maximum rig capacity.

Stressing was performed using a 100 tonne capacity centre-hole actuator manufactured by Simplex-Pine (model RC1110C). The stroke of the actuator was 250 mm - adequate to cope with the extension from an 8 metre long Type G Parafil rope. The actuator was powered by an electrically operated 10 000 p.s.i. Amsler hydraulic cabinet.

Large amounts of strain energy are dissipated when ropes are tested to failure. For this reason, the stressing equipment was tied back to the box-section. Four 0.5 inch diameter mild steel bolts proved

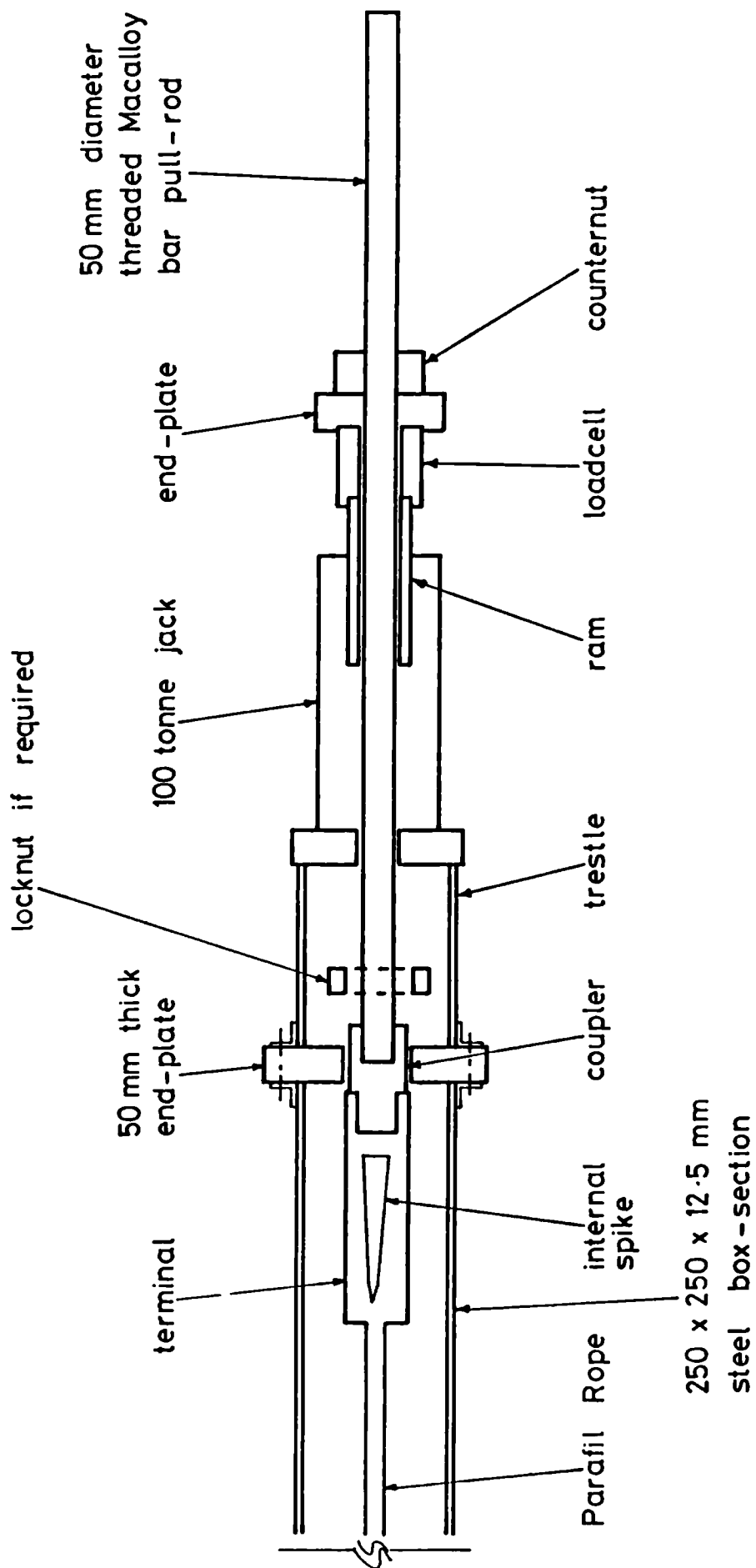


Figure 4.3 Prestressing system.

sufficient to prevent the equipment from becoming detached from the rig.

The dead-end anchorage is shown in Figure 4.2. The existing external thread of the terminal was extended slightly to allow for some tolerance in rope positioning. A threaded mild steel locknut of 150 mm external diameter, bearing against an end-plate completed the anchorage. Mild steel end-plates (see Figure 4.3) of 50 mm thickness were found to be structurally adequate.

4.4 Measurement of results and instrumentation

4.4.1 Introduction

During the tension tests it was necessary to record the voltages from four instruments for each set of readings. Output voltages were recorded from:

- (1) a loadcell measuring the force in the rope (4.4.3)
- (2) a transducer measuring total displacement (4.4.4)
- (3) a transducer measuring the bed-down of the steel spike in the dead-end terminal (4.4.5)
- (4) a strain-gauge bridge located on the box-section (the bridge was used as a source of load measurement in later tests) (4.4.6).

4.4.2 Data logger

All test measurements were recorded using a Hewlett Packard (HP) 3054A automatic data acquisition control system (Figure 4.4). The system consists of a high speed scanner and a high resolution digital voltmeter (model 3456A); these were interfaced with an HP85 desktop computer. Either automatic readings, achieved through the use of specially written software, or manual readings may be taken with this system. However, owing to the nature of the tension tests, in which the steadily increasing load caused the instrument voltages to change continuously all measurements had to be taken automatically.

The 3456A digital voltmeter offers variable speeds and reading

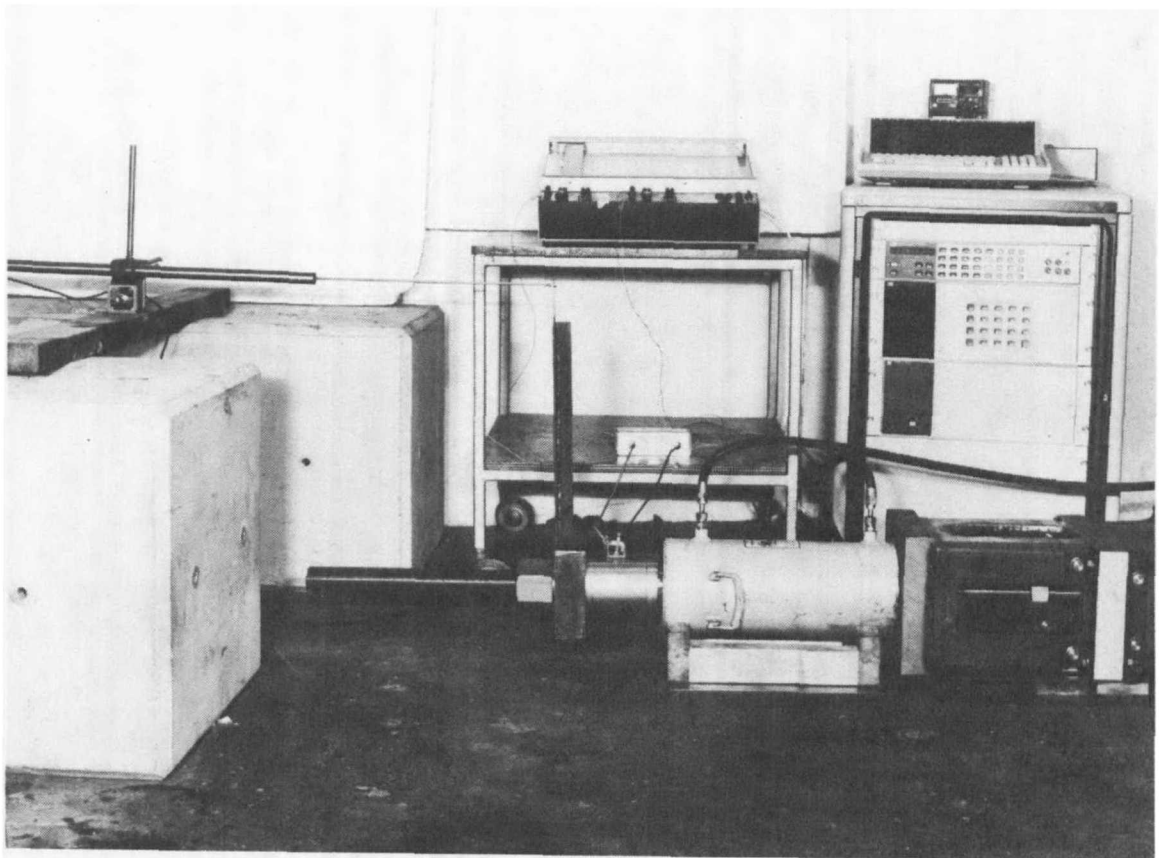


Figure 4.4 View of live-end with HP data logger and X-Y plotter in background.

precision. A maximum reading rate of 330 readings/second is claimed by the manufacturer but this may only be achieved in specific circumstances and with the loss of some accuracy. Maximum voltmeter sensitivity (obtained at much slower reading rates) is 100 nanovolts. In practice, the correct balance between speed and accuracy needs to be made depending on the type of measurements.

Serious consideration was given to the speed and method of taking readings during the tension tests, therefore, to ensure that the readings were not only accurate but more importantly 'simultaneous'. A comparison was made between the different methods of taking readings (the methods can be changed by modifying the computer software). The comparison revealed two problems. Firstly, significant delays occurred between individual voltage readings when they were transferred directly from the voltmeter to the computer memory. Secondly, the manipulation and printing of data caused long delays between successive sets of readings.

The use of the voltmeter's internal memory gave a solution to the first problem; its use caused a marginal drop in voltmeter sensitivity (down to 10 microvolts on the 10 volt range) but enabled voltages to be read and stored in only 25 milliseconds. The second problem was solved by simply leaving the manipulation and printing of all data until after test completion; besides, it was possible to monitor the progress of tests without printed data through the X-Y plotter (4.4.7). Despite the much improved voltage reading-rate (40 voltages per second) it was possible to make one further improvement. This involved closing the four voltage channels, first in one order and then in reverse, and taking an average of the output voltages, such that the readings were essentially 'simultaneous'.

4.4.3 Loadcell

Measurements of load were taken using a purpose-designed 100 tonne loadcell made from aluminium (HE15 Duralumin S). The loadcell was cylindrical in shape and had a wall-thickness of 16 mm. It was positioned between the ram of the actuator and an end-plate as shown in Figure 4.3. The end-caps of the loadcell were shaped to give a

close-fit with the ram and the end-plate and thus ensure that the loadcell was loaded as uniformly as possible.

Duralumin S is commonly used for loadcells since it has a highly reproduceable stress-strain curve and at suitable stress-levels will cause significant changes in output voltage when strain-gauged. Eight electrical-resistance strain gauges were used in a full Wheatstone bridge arrangement; this configuration compensates automatically for any bending strains (these would arise if there were any eccentricity in the applied loading). The bridge was powered by a constant direct current 10 volt power source.

The loadcell was exercised several times in a compression machine prior to calibration. A strut cell, with a known calibration factor (determined at the National Physical Laboratory) was used as a reference source of load. Four calibration runs were performed; the calibration factor determined from each set of results was sensibly constant and so an average calibration factor was adopted. Care was taken to rotate the loadcell through 90° between successive runs and also to account for the small variation in input voltage when determining calibration factors. The accuracy of the loadcell, governed by the sensitivity of the digital voltmeter, was ± 0.2 kN.

4.4.4 Rope displacement

It was not feasible to measure rope displacements or strains directly for two reasons:

(1) The Kevlar 49 yarns are encapsulated by an outer sheath which is non-loadbearing and may well slip relative to the core. The attachment of strain gauges or a displacement transducer to the outer sheath would, therefore, probably have led to erroneous results.

(2) The removal of the outer sheath, except at rope ends, can easily lead to damage of the rope core; this essentially precluded the possibility of attaching instrumentation to the core itself. If a section of the outer sheath were to have been removed successfully then the attachment of a displacement transducer to the exposed core may have been possible. It would certainly have been impossible, however, to attach strain gauges to the bare yarns.

One displacement which could be measured easily was that of the ram of the jack relative to a fixed position on the laboratory floor. A linear variable differential transformer (LVDT) manufactured by Solartron Schlumberger and having a 300 mm stroke was chosen for this purpose. Since the LVDT measured overall displacements rather than actual rope extensions, allowances had to be made for the bed-down of the rope within the fittings and for the small extension of the pull-rod when determining the true material response (the method of articulation of the box-section relative to the LVDT meant that the elastic compression of the box-section was inconsequential). The allowance for bed-down was made by monitoring the progressive displacement of the steel spike in the dead-end terminal (4.4.5); pull-rod extensions were calculated on the basis of load measurements and a published value for the axial stiffness of the Macalloy bar. Both corrections were automatically accounted for in the software.

The transducer was calibrated three times against a one metre vernier scale mounted horizontally; the transducer was rotated through 120° between successive runs. Output voltages, measured using the HP data acquisition system, were read every 10 mm, up to 160 mm either side of the null voltage. A computer program, designed to interrogate the data, ensured that the calibration factors were determined from the linear range of the transducer only; this usually meant that the first and last couple of readings were ignored. All three factors were reasonably constant; an average of the runs was thus adopted. An accuracy of $\pm 0.025\text{mm}$ was found to exist.

4.4.5 Bed-down within fittings

Parafil rope terminations are based on a conical wedge principle (3.5.4) and so it is inevitable that some bed-down of ropes will occur when loads are applied. It is important to account for this bed-down when estimating the length of rope required for a practical application. For this reason ICI have published approximate figures for the bed-down of ropes within fittings, but only for ropes up to 20 tonnes nominal breaking load (NBL). Results for 60 tonne ropes had not been evaluated although an estimate based on the figures for the smaller ropes could probably be made. More importantly, however, from

the point of view of studying actual material behaviour during these tension tests, it was imperative to monitor the bed-down of ropes within fittings continuously (as opposed to measuring the overall amount) since it was not feasible to measure rope displacements directly.

The movement of the steel spike within the dead end rope terminal (Figure 4.2) was monitored with a 25 mm stroke LVDT. The piston of the LVDT was spring-loaded which ensured that positive contact was maintained between the instrument and the steel spike throughout the tests. It was not feasible to measure the bed-down of ropes continuously in the live-end terminal, however, although occasional measurements could be made with a micrometer depth-gauge when the loading mechanism was removed (4.5.2).

The 25 mm stroke LVDT was calibrated in a similar manner to that described previously except that a 50 mm circular vernier scale, with divisions of 0.002 mm, was used. The accuracy of this transducer was found to be ± 0.005 mm.

4.4.6 Box-section gauging

An aluminium loadcell is not suitable as a means of load measurement in long-term tests owing to problems with creep. For the stress-rupture tests (Chapter 6), four electrical resistance foil-type strain gauges, arranged in a full Wheatstone bridge configuration, were used as an alternative means of load measurement. One strain gauge was glued to the centre of each of the four sides of the main box-section, half-way along its length. Although the box-section was approximately seventy times stiffer than the Parafil ropes it was still sufficiently flexible to cause a moderate change in output voltage from the bridge.

Advantage was taken of the tension tests to calibrate the strain-gauge bridge against the loadcell. The accuracy of the bridge was estimated to be $\pm 1\%$ of the nominal breaking load.

For the stress-relaxation tests (Chapter 5) vibrating wire gauges were used to monitor the decay in load. These gauges are described in Section 5.4.

4.4.7 X-Y plotter

During the tension tests the output voltages from the loadcell and the 300 mm stroke LVDT (measuring total displacement) were fed directly to an X-Y plotter (Bryans 29000 A3) as well as to the HP data acquisition system. The vertical and horizontal axes of the plotter were calibrated against their respective instruments to enable determinations of load and displacement to be made.

It was important to use the X-Y plotter for three reasons:

- (1) It enabled the peak load and displacement to be 'captured' (the time lapse between sets of readings meant that the HP data logger would always underestimate peak values).
- (2) It gave a continuous trace of load versus displacement as the tests proceeded which helped to highlight areas of special interest.
- (3) It gave an immediate indication of any problems associated with the loading which was useful in terms of safety.

4.5 Test procedure, results and discussion

4.5.1 Introduction

Tension test results have been obtained for seven 60 tonne Type G Parafil ropes. The sample included ropes from three different production batches. The initial rope length in each test was 7850 mm. The tests were essentially displacement controlled, the rate of displacement being held constant at 1.31 mm/second. This was the maximum rate of displacement that could be provided by the hydraulic cabinet.

4.5.2 Pretensioning

All ropes were first pretensioned in the manufacturer's prescribed manner. This entailed loading the ropes to 60% of their nominal breaking load (i.e. 36 tonnes), unloading, and leaving for one hour before testing. Parafil ropes are supplied on circular drums (drum diameter = 50 x rope diameter); pretensioning is intended to remove any minor disorientation of the fibre lay caused by the coiling process. The small amount of permanent deformation attributable to the coiling process is known as the disorientation effect.

Ropes were drawn handtight before the application of any load. This was achieved by pulling manually on the Macalloy bar pull-rod and taking up the slack with the counter nut. Owing to the handling stiffness of the rope, however, it was probable that some slack remained before stressing began.

The position of the spike within the terminal at the stressing end was noted prior to loading. A micrometer depth gauge was used for this purpose. The spike position was again determined after pretensioning (and also after testing to failure). It was impossible to monitor the displacement of the spike within the live-end fitting continuously owing to its inaccessible location.

Figure 4.5 shows a typical load-displacement curve for the rope during pretensioning. Large displacements of the rope were measured before it began to carry any appreciable load. From then on, the stiffness of the rope increased progressively up to the pretensioning limit. Upon load removal, a permanent offset, Δp , was observed on the displacement axis.

Rope displacements were determined automatically in the software from the following equation:

$$\text{rope displ.} = (\text{total displ.}) - (\text{total bed-down}) - (\text{pull-rod extension})$$

Total displacements were measured using the 300 mm stroke LVDT described earlier. Bed-down within the terminal at the dead-end was measured continuously with the smaller LVDT. The bed-down in both

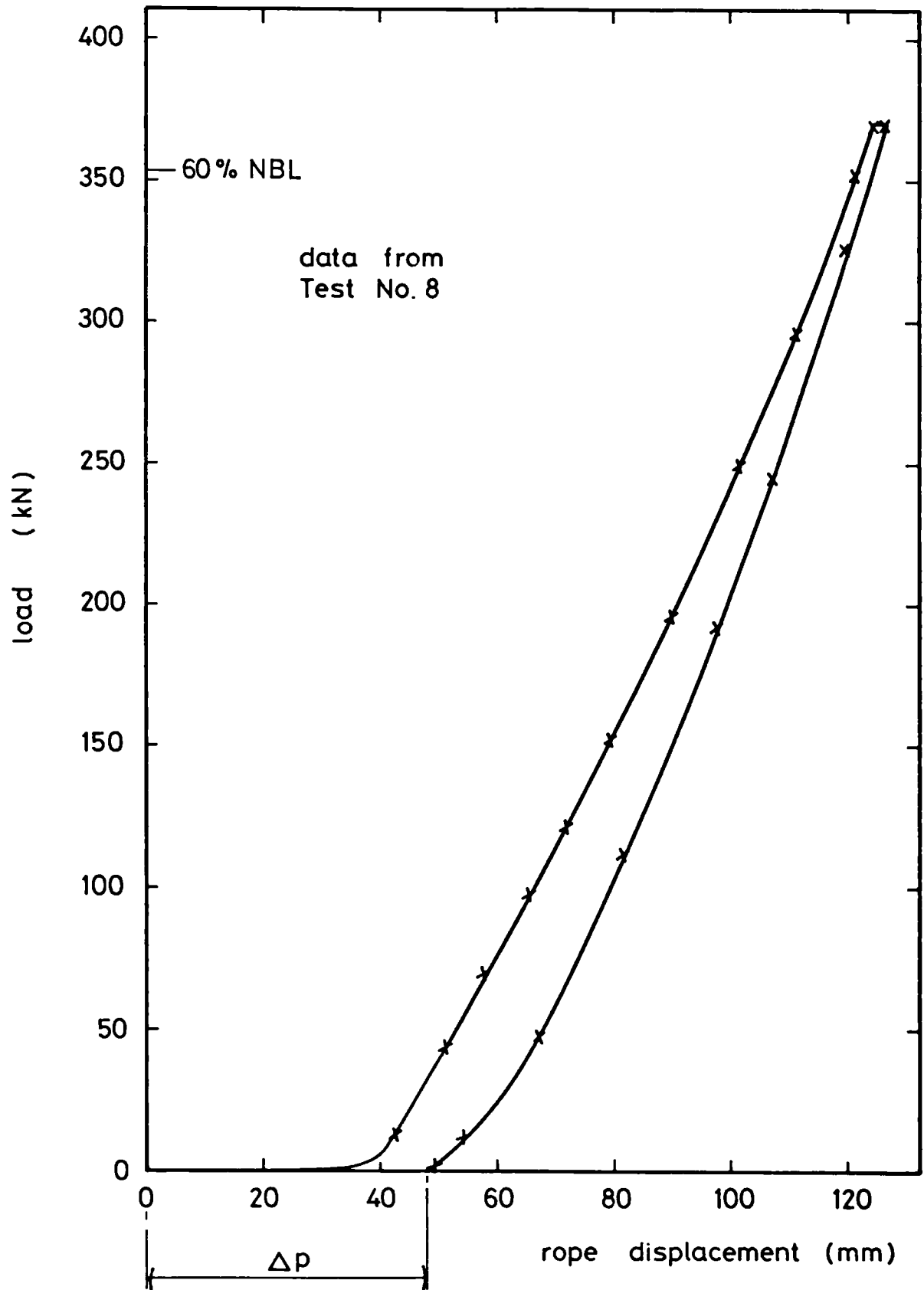


Figure 4.5 Typical load-displacement curve from pretensioning.

terminals, at any load level, was derived from the following equation:

$$\text{total bed-down} = b_{\ell} + \left(\frac{b_1}{b_2} \times b_{\ell} \right)$$

where: b_{ℓ} is the bed-down in dead-end terminal under load ℓ (LVDT)
 b_1 is the final bed-down in live-end terminal (micrometer depth gauge).
 b_2 is the final bed-down in dead-end terminal (LVDT).

Pull-rod extensions are small (only 1.6 mm at 60 tonnes) but they are easily accounted for in a computer program. Hence, these were also taken into consideration when determining values for rope displacement.

The permanent offset, Δp (Figure 4.5), is made up of two components: initial slack within the rope plus the coiling or disorientation effect. Values of initial slack may be estimated from the individual rope load-displacement curves but owing to the shallowness of the initial part of the curves, these estimations are rather subjective. Nevertheless, approximate values for the disorientation effect have been calculated on this basis and are indicated in Table 4.1. Disorientation strains are quoted on the assumption that the effective length of the rope is 7400 mm. This length is the approximate distance between spike tips and is the distance over which the yarns of the rope must be subject to full tension.

Values for spike bed-down attributable to pretensioning are shown in Table 4.2. The amount of bed-down is dependent on how hard the spike is 'knocked-home' initially; it is preferable to hammer the spike gently and allow the pretensioning load to draw the spike uniformly into the body of the terminal. In this manner, the possible occurrence of yarn damage or yarn bunching is precluded.

Test number	Permanent offset, Δp (mm)	Estimated initial slack (mm)	Disorientation effect (mm)	Disorientation strain (%)
3	56.4	37.4	19.0	0.26
4	58.6	38.2	20.4	0.28
5	53.1	32.8	20.3	0.27
7	58.2	36.7	21.5	0.29
8	48.2	26.7	21.5	0.29
9	53.4	29.4	24.0	0.32
10	49.2	32.2	17.0	0.23

Table 4.1 Components of displacement offset, Δp , after pretensioning.

Test Number	Bed-down, measured at zero load after pretensioning (mm)	
	dead-end (b_2)	live-end (b_1)
3	16.32	14.42
4	15.52	15.26
5	15.27	14.49
7	22.93	23.90
8	24.84	23.91
9	25.29	25.20
10	24.64	24.13

Table 4.2 Bed-down within fittings due to pretensioning.

A typical plot of load against the spike bed-down that occurs within a single terminal during pretensioning is shown in Figure 4.6. The rate

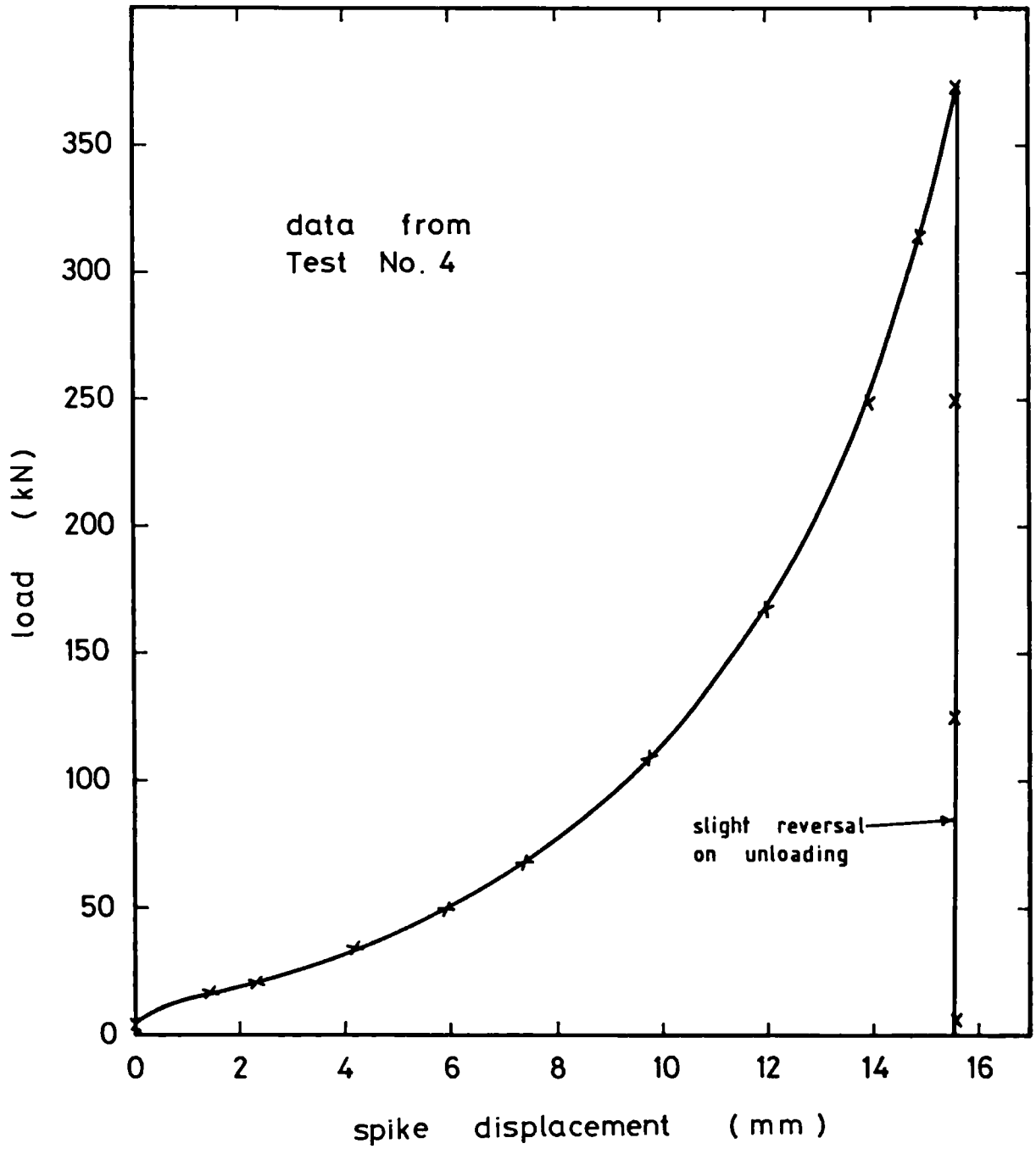


Figure 4.6 Typical plot of bed-down within a single terminal due to pretensioning.

of movement of the spike decreases rapidly as the load is applied such that most bed-down occurs at only small loads.

The relative position of the end of the spike and the small exposed length of rope yarns was noted before and after pretensioning, in each test. The position was found to be unchanged indicating that no slip had occurred between rope yarns and the spike. This confirmed that rope bed-down was equivalent to spike bed-down.

4.5.3 Testing to failure

Ropes were tested to failure one hour after pretensioning. The same rate of displacement (1.31 mm/sec) was employed. Typical load-displacement curves are shown in Figures 4.7 and 4.8. Table 4.3 gives principal test results.

The material response may be described as bi-linear. A slight stiffening occurs at a load of approximately 325 kN; this load is close to the pretensioning limit. The material exhibits a brittle failure and little or no plastic deformation is displayed.

Considerable strain energy is stored within Parafil ropes at the onset of failure, yet the ropes were safely contained within the box-section after fracture despite sudden failures. It is believed that the majority of the strain energy is dissipated in the production of fracture surfaces (2.3). Also, the outer sheath of the ropes helps to contain the energy of the break. Parafil ropes are, therefore, inherently safe despite their brittle mode of failure.

The curvature at the beginning of the load-displacement plots (Figures 4.7, 4.8) is attributable to rope catenary effects; these are unavoidable when ropes are tested horizontally. Appendix 2 indicates that catenary effects will produce an apparent slack of between 3 mm and 14 mm if ropes are initially pulled handtight. Extrapolations of the virtually linear load-displacement curves from the point of stiffening (325 kN) produce offsets on the displacement axis within this range. This confirms that the initial curvature is due to catenary effects and not actual material behaviour.

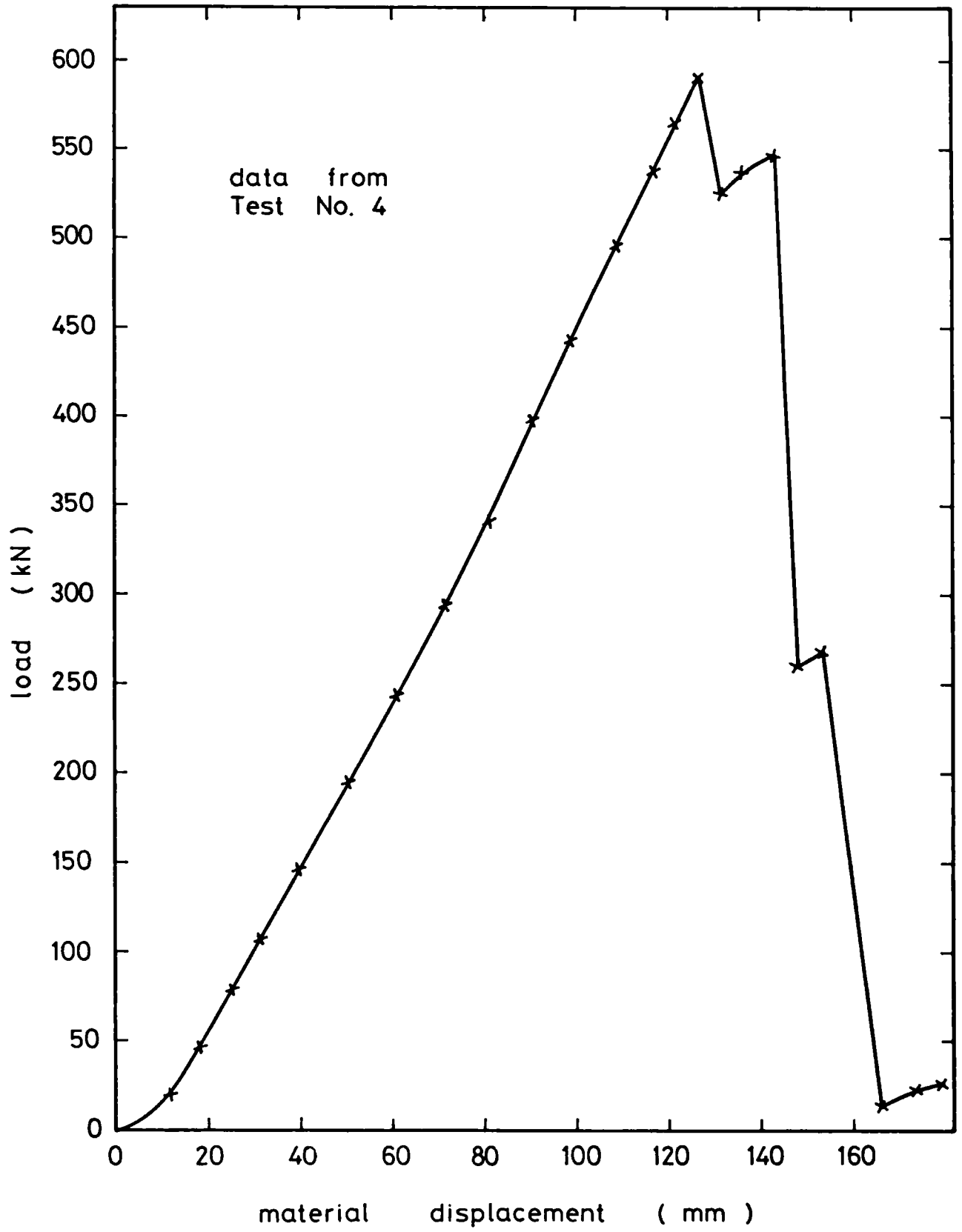


Figure 4.7 Typical load-displacement curve.

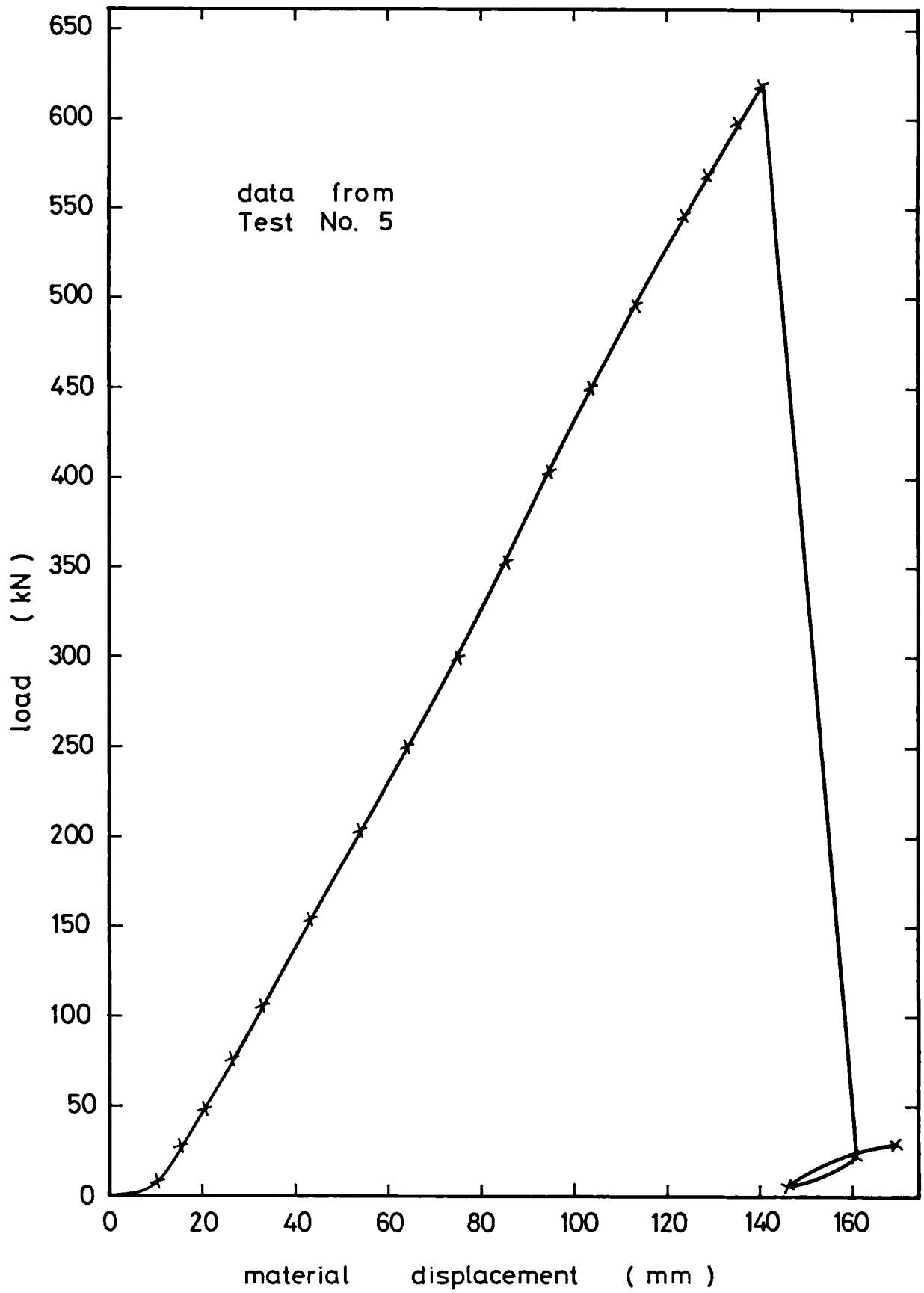


Figure 4.8 Typical load-displacement curve.

Test number	3	4	5	7	8	9	10	Means
Ultimate load (kN)	566.8	589.7	619.2	626.6	592.1	587.2	597.4	597.0
Ultimate stress (MPa)	1855	1930	2026	2050	1938	1921	1955	1954
Maximum displacement (mm) (includes slack)	129.9	127.9	138.8	131.7	131.7	127.9	132.9	131.5
Initial slack (mm)	9.3	8.5	10.1	5.3	10.5	6.7	10.4	8.7
Corrected max. displacement (mm)	120.6	119.4	128.7	126.4	121.2	121.2	122.5	122.9
Ultimate strain (%)	1.63	1.61	1.74	1.71	1.64	1.64	1.65	1.66
Overall modulus (GPa)	114	120	116	120	118	117	118	118
Approx. load for first audible yarn failure (kN)	415	415	355	>400	475	450	525	434

Notes: tests 3, 4, 5 from batch 1 - Run number 1021/83
test 7 from batch 2 - Run number 1183/83
tests 8, 9, 10 from batch 3 - Run number 1005/85

tests are displacement controlled, rate = 1.31 mm/sec
initial rope length = 7850 mm
effective rope length for strains taken as 7400 mm.

Table 4.3 Principal tension test results.

Large amounts of initial slack, as observed during pretensioning (Table 4.1) were not revealed when testing ropes to failure. The difference in behaviour may be due to the difficulty of removing any initial kinks in the ropes by hand, prior to pretensioning, owing to the high handling stiffness of the material. Alternatively, it could be argued that the initial slack observed in pretensioning includes some constructional rope stretch which is subsequently removed by the pretensioning load.

Figures 4.7 and 4.8 illustrate the two failure modes that were observed in the tension tests. While the test rig was not designed specifically to study the descending path of the load-displacement curves (assuming this to be possible) Figure 4.7 clearly shows failure occurring in a series of diminishing peaks. Sixty tonne ropes consist of a number of separate yarn bundles; it is probable that the peaks reflect the progressive failure of individual bundles as load is redistributed and hence indicate that load-sharing between bundles is imperfect. Of the seven ropes tested, five displayed secondary peaks on the load-displacement curve. There seemed to be no connection, however, between the presence of secondary peaks and observed values of ultimate strength; this may be attributable to the sample size.

Figure 4.9 indicates the results of Test Number 5 plotted in terms of stress and strain and ignoring the initial curvature due to catenary effects. In calculating strains an effective rope length of 7.4 metres has been assumed.

The moduli values quoted in Table 4.3 are simply ratios of ultimate stress to ultimate strain. This seems an appropriate basis for the determination of the elastic modulus since Figure 4.9 reveals that the difference in moduli values for the two portions of the bi-linear behaviour is small. However, a smaller mean value for the modulus of the material is achieved than commercially quoted (118 GPa c.f. 127 GPa). Indeed, it would not be possible to attain the higher figure even if the full rope length were used in the calculations.

When stressing ropes to failure it was possible to hear yarns snap, the box-section acting as an excellent sound box. The first audible indication of a yarn failing typically occurred above 70% of the

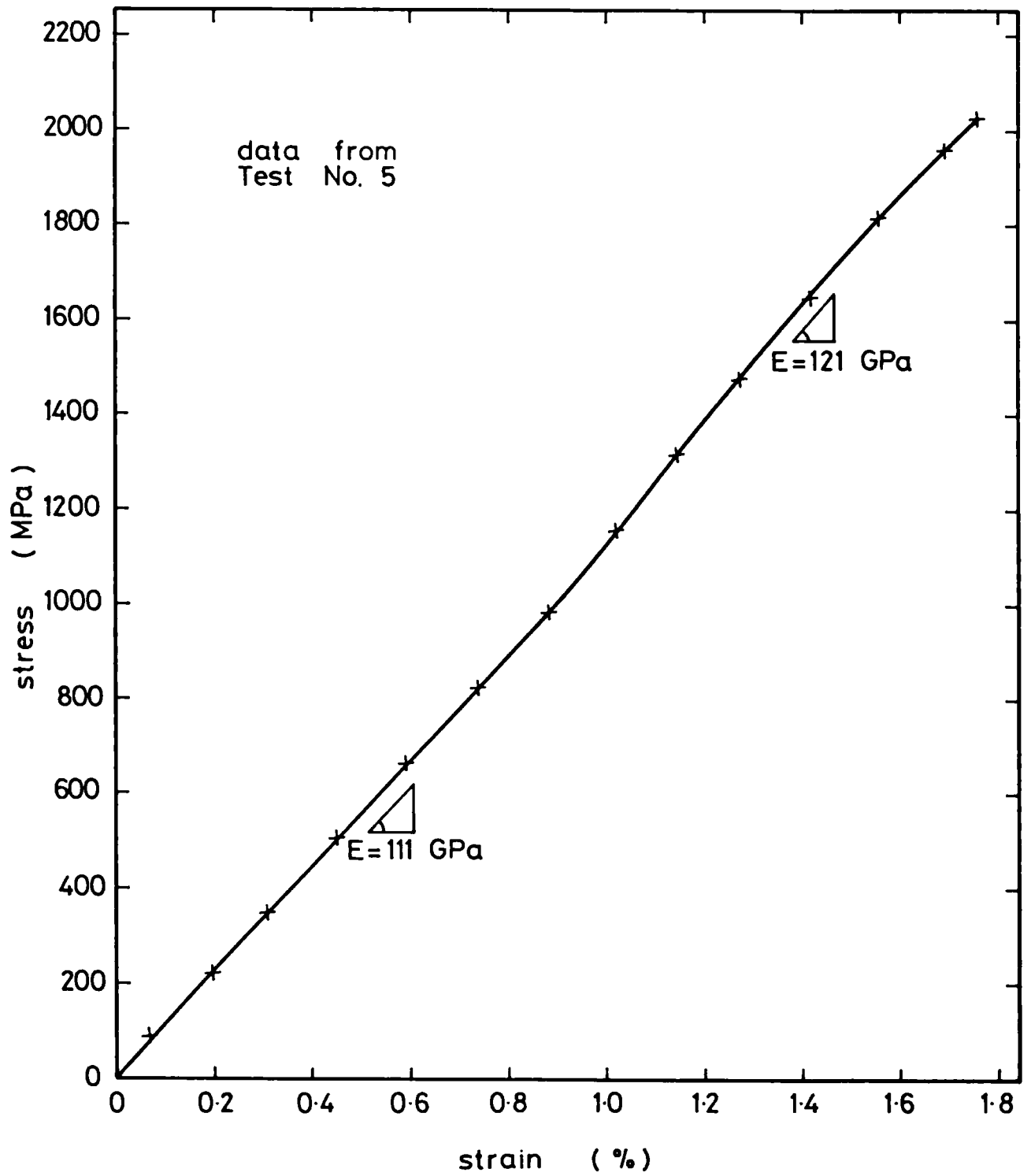


Figure 4.9 Typical stress-strain curve.

nominal breaking load. The relatively low load at which yarn failure became evident is likely to influence the long-term durability of the ropes.

The outer sheaths from Test Numbers 3,4 and 5 were removed after rope failure so as to gain some insight into the mode and position of failure. In all cases failure had occurred towards the longitudinal centre of the rope; yarns had severed over a length of around 1.0 to 1.5 metres. In one test yarns had snapped in two locations; this may indicate that after the first fracture sufficient frictional restraint was developed with neighbouring yarns to mobilise further load-carrying capacity. The development of friction between yarns provides a possible alternative explanation as to why secondary peaks were observed in the descending paths of some of the load-displacement curves. However, in support of the original argument, there was also some visual evidence that rope yarns had failed in distinct bundles. Surprisingly, about one-third of the yarns within ropes remained intact although these appeared, at the end of tests, to have only a nominal load-carrying ability.

The bed-down of the steel spike in the dead-end terminal was monitored as previously described. Table 4.4 indicates that only small amounts of bed-down occur when testing ropes to failure when compared to the amounts that occur during pretensioning. Figure 4.10 indicates how a steel spike moves during a typical tension test (including the pretensioning). As expected, in the ultimate strength test, the majority of the additional bed-down occurs once the pretensioning limit (60% NBL) has been exceeded.

A small amount of spike slip occurs within terminals at load levels slightly less than 60% of the nominal breaking load. A mechanism for slip might be explained in terms of stresses within the termination. The spike remains jammed in position after pretensioning (Figure 4.10) due to the lateral forces imposed on it by the terminal body; the lateral forces in turn cause a large frictional restraint against backward movement to be established. Correspondingly, the spike causes tensile ring-stresses to be set-up in the terminal body which remain after the pretensioning load has been removed. Consequently, during tension tests, only very small amounts of additional bed-down

Test number	Additional bed-down of steel spike in dead-end terminal at peak load (mm)	Bed-down in dead-end terminal due to pretensioning (mm)
3	5.3	16.3
4	6.0	15.5
5	6.5	15.3
7	4.1	22.9
8	3.6	24.8
9	3.5	25.3
10	3.3	24.6

Table 4.4 Bed-down values for dead-end terminal.

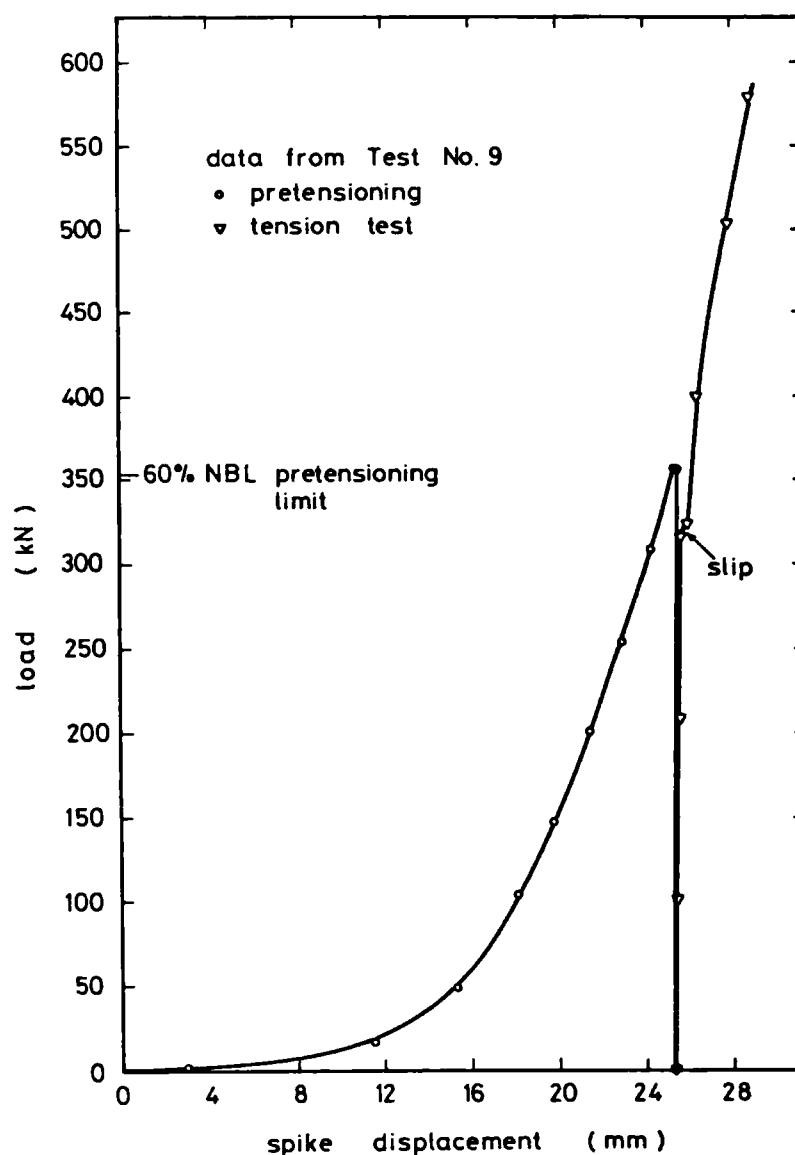


Figure 4.10 Total movement of a steel spike due to rope pretensioning and loading to ultimate.

are observed until the residual ring-stress is exceeded; when this stress is exceeded some slip might be expected and is indeed observed in practice. Furthermore, since one hour elapses between the pretensioning of ropes and their testing to failure, the residual ring-stress would decay slightly; hence, the slip of the spike occurs at a load slightly less than the pretensioning limit.

There is a slight reversal of the spike at failure due to shock effects. The degree of reversal was variable and for this reason it was assumed that the bed-down in the two terminals was equal when determining material displacements from total displacements. Table 4.2 showed that bed-down within the dead-end and live-end terminals during pretensioning was approximately equal. Also, the amount of additional bed-down that occurs when testing ropes to failure is small; hence, the error incurred from adopting the assumption of equal bed-down is negligible.

4.6 Statistics

Daniels (1945) was the first to show that as the number of yarns in a bundle grows large so the bundle strength theoretically becomes asymptotically normally distributed. In agreement with Daniels' observations it will later be shown (Chapter 8) that the strength of 6 tonne Type G Parafil ropes follows a normal distribution. Hence, if it is assumed that:

(1) the distribution of tensile strength for 60 tonne Type G ropes is also Gaussian (normal) and,

(2) the sample of seven ropes tested is random and representative of the population,

then it may be stated with 95% confidence that for a rope with a quoted nominal breaking load of 60 tonnes, the mean strength (μ) lies in the range 58.95 tonnes to 62.78 tonnes. This range has been determined using the well-known t distribution.

It is necessary, in later chapters, to refer to percentages of load and also to normalise with respect to the rope breaking strength. Since the sample mean breaking load ($\bar{x} = 60.88$ tonnes) is only 1.5% greater than the nominal breaking load and the nominal breaking load

itself lies within the confidence limits, it would seem experimentally justified and also less confusing to adopt 60 tonnes as the true rope breaking strength for these purposes.

The standard deviation of the sample (s) is 20.2 kN. The corresponding coefficient of variation is 3.4%. If a chi-square distribution is adopted then the range for the standard deviation of the population (σ) at a 95% level of confidence, is given by:

$$13.0 \leq \sigma \leq 44.5 \quad (\text{kN})$$

Engineers usually deal in terms of characteristic strength rather than mean strength. The characteristic strength represents a strength below which not more than 5% of results will fall and it forms the basis for the derivation of engineering design strengths. Characteristic strengths should be calculated from the population parameters (μ, σ) but these may only be determined accurately if a large sample size is chosen (see Chapter 8). A large sample size would be inappropriate for 60 tonne ropes owing to their size and cost. However, if sample parameters ($\bar{x}, s$) are used instead then the characteristic strength is estimated to be 56.7 kN (57.48 tonnes). A summary of statistical strength results is given in Table 4.5.

Test number	Ultimate load (kN)			
		sample parameters	population parameters at 95% confidence	distribution
3	566.8	$\bar{x} = 597.0$	$578.3 \leq \mu \leq 615.7$	t
4	589.7			
5	619.2			
7	626.6			
8	592.1	s = 20.2	$13.0 \leq \sigma \leq 44.5$	chi-square
9	587.2			
10	597.4			
sample coefficient of variation ($s/\bar{x}$) = 3.4%				
sample characteristic strength ($\bar{x} - 1.645s$) = 563.7 kN				

Table 4.5 Tensile strength statistics for 60 tonne Type G ropes.

CHAPTER 5: STRESS-RELAXATION

5.1 Introduction

The long-term time-dependent behaviour of Parafil ropes is extremely important since prestressing tendons are permanently highly stressed. The properties of prime significance are stress-rupture and stress-relaxation. The stress-rupture behaviour of Parafil will strongly influence the chosen operating stresses for the material; this property is considered in depth in Chapters 6 and 7. Knowledge of the stress-relaxation behaviour of Parafil is also required, however, so that sufficient prestress can be applied to a prestressed concrete member to guarantee its long-term structural integrity.

Stress-relaxation is defined as the loss of stress (with time) at constant strain. It is essentially a creep phenomenon, creep being defined as the increase in strain at constant stress. Stress-relaxation and creep, however, represent two extreme time-dependent material characteristics; in practice engineering materials are rarely held at constant strain or subject to constant stress. In particular, the length of prestressed concrete members does not remain constant but reduces with time due to shrinkage and creep of the concrete; hence, it would be useful in experiments to monitor the decay of stress in a Parafil rope when subject to a decreasing strain. However, it is widely accepted that a constant strain test (i.e. a stress-relaxation test) is the only test that can sensibly be performed.

This chapter includes a short review of the prestress losses that will have to be taken into consideration when designing members based on Parafil tendons. Some earlier work on the stress-relaxation of Kevlar is then described; this is rather limited since stress-relaxation is not a commonly measured material property. The results of the stress-relaxation tests on Parafil Type G ropes carried out in this project are then reported and discussed. Finally, a recommendation for further research is made followed by a comparison between typical prestress losses arising from the use of Parafil and steel tendons.

5.2 Prestress losses

The initial prestress applied to concrete members is reduced significantly with time due to many well-documented factors (see, for example, Abeles and Bardhan-Roy 1981). Some prestress losses occur instantaneously or within a short time of the application of prestress but others continue to increase (albeit at reducing rates) over the life of the structure. For structures to remain serviceable throughout their design lives, which in the case of bridges may be as long as 120 years, an accurate estimate of the long-term prestress loss has to be made. The necessary amount of initial prestress that needs to be applied can then be determined accordingly.

When estimating the residual stress in a prestressing tendon it is convenient to consider prestress losses as arising from the following distinct factors:

- (1) concrete creep
- (2) concrete shrinkage
- (3) elastic deformation of the concrete
- (4) slip at the anchorage
- (5) friction
- (6) stress-relaxation of the tendon.

If it is assumed that the stress-rupture characteristics of Parafil are known (Chapters 6, 7) then it only remains to determine the stress-relaxation behaviour of Parafil to enable prestressed concrete members incorporating Parafil Type G tendons to be safely designed.

For instance, the prestress losses arising from concrete creep, shrinkage and elastic deformation can be assessed in the manner usually adopted for steel tendons but using the elastic modulus for Parafil. (It appears that the elastic modulus of Parafil increases slightly with time under stress (5.8) but this will not significantly affect any assessment of the prestress losses due to these causes which are at best approximate.)

Prestress losses due to anchorage slip are inevitable in prestressing systems designed for steel tendons which rely on wedges and sockets. However, anchorage slip in the Parafil system (in the form of spike

bed-down) can be restricted to the pretensioning process (5.5) performed in advance of prestress transfer. When the prestressing load is actually transferred to the concrete a threaded locknut is used which, as would be expected, does not lead to any loss of stress (5.4).

For a steel tendon, the loss of prestress due to friction is caused by contact between the tendon and the prestressing duct. For Parafil, friction between the core yarns and the outer sheath, and also between the outer sheath and the prestressing duct would have to be considered. While a direct investigation of the frictional losses associated with Parafil remains outstanding, Guimares (1985) has begun a programme of work in which the friction between the inner core of yarns and various types of outer sheath is being investigated; once completed, it should be possible to make a better approximation of frictional losses. It is also possible that with a careful choice of materials for the outer sheath and the prestressing duct the loss of prestress due to friction for Parafil tendons could be kept to insignificant levels.

5.3 Earlier work on stress-relaxation

Only a few researchers appear to have studied the stress-relaxation behaviour of Kevlar and in all cases the work has been confined to Kevlar yarns or filaments. Inconsistencies within the results of each particular investigation are common. There is also disagreement regarding the degree of dependence of stress-relaxation on the initially applied stress.

Du Pont (1976) have investigated the stress-relaxation behaviour of Kevlar 29 resin impregnated strands when subject to initial stresses ranging from 70% to 90% of the ultimate tensile strength. The results are shown in Figure 5.1; a linear dependence of load loss with the logarithm of time was observed. Interestingly, the rate of loss of load was found to be independent of the initially applied stress. The specimens which were not allowed to creep to failure but which were removed from the test rig and tested in tension, exhibited (on average) 100% strength-retention.

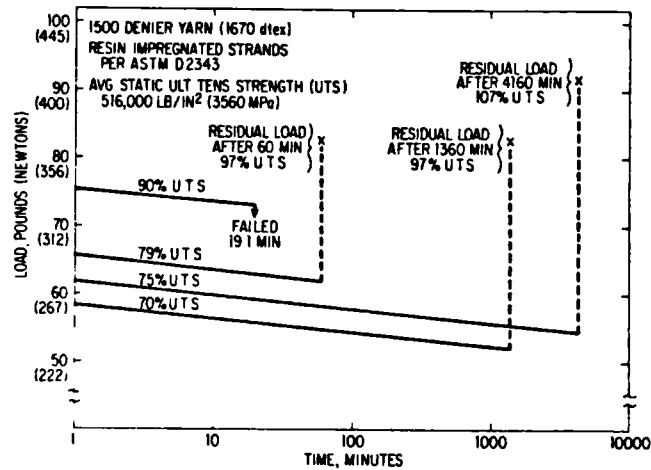


Figure 5.1 Stress-relaxation of Kevlar 29 resin impregnated strands (Du Pont 1976).

Schaeffgen (1983), on the other hand, reports that stress-relaxation for Kevlar is stress-dependent and may be defined by:

$$s = s_1 S \log t$$

where: s is the stress loss
 s_1 is the initial stress
 S is the stress-relaxation coefficient
 $= 0.016$ for Kevlar 49
 $= 0.024$ for Kevlar 29
 t is the time in minutes.

The equation indicates that relaxation is directly proportional to the initial stress which conflicts with the results shown in Figure 5.1.

Ko (1980) has also performed stress-relaxation tests on Kevlar 49 fibres as part of an attempt to characterise their viscoelastic behaviour. A large range of stress was considered (see Figure 5.2) but the test duration was limited to 1000 seconds; even so, a linear dependence of the load loss with the logarithm of time soon developed. The results indicate a fairly wide scatter of relaxation values at each stress level and only a marginal dependence of the relaxation rate with stress.

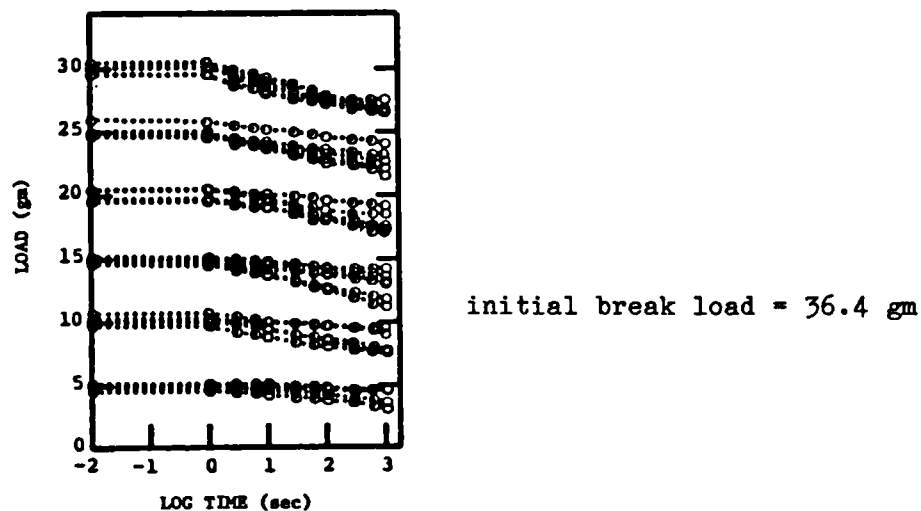


Figure 5.2 Stress-relaxation of Kevlar 49 filaments (Ko 1980).

Cook et al.(1982) have carried out considerable research into the stress-rupture and creep behaviour of Kevlar 49 yarns using a split-ring device. The device imposes a constant strain on the yarns rather than a constant stress and so stress-relaxation results are furnished. The results (shown in Table 5.1) indicate the short-term relaxation of Kevlar 49 under various initial loads. A relaxation of about 7% of the initial stress typically occurred within the first 300 seconds. Subsequent relaxation was found to be generally linear with the logarithm of time and essentially independent of stress.

Peak fibre stress GPa	mean % relaxation	mean time (s) (half relaxed)	No of samples
0.14	5.1	14	1
0.40	7.8	3.4	5
0.46	7.3	3.4	4
0.56	7.9	4.0	4
0.83	7.0	7.2	3
1.05	5.7	11.8	1

mean fibre strength = 2.58 GPa

Table 5.1 Stress-relaxation in first 300 secs at 20° C
for Kevlar 49 yarns (Cook et al.1982).

5.4 Test procedure and instrumentation

The stress-relaxation tests on Parafil Type G ropes were performed within the steel box-section described earlier (4.2). They were conducted in a controlled environment with the laboratory temperature found to be $20^{\circ} \pm 2^{\circ}\text{C}$. All ropes were pretensioned before testing (4.5.2) but the limit of pretensioning was varied slightly to eliminate bed-down effects (5.5).

The maximum compressive stress in the box-section, whilst a test was in progress, was approximately 35 MPa. The creep of steel at this level of stress is negligible (indeed no data appears to be available) and this was verified for the box-section in a constant load creep test. The strain in the ropes throughout this series of tests was, therefore, essentially constant.

The same method of stressing was used as in the tension tests; however, in this test series, a locknut was loosely located over the pull-rod prior to stressing. During the loading phase, the locknut was threaded onto the external thread of the rope terminal once it began to project from the box-section end-plate. When the desired load had been reached the locknut was tightened with a 'tommy' bar against the end-plate (windows in the steel trestle enabled access to be gained to the locknut - see Figure 4.3). Finally, the pressure in the actuator was released, thus 'locking' the stress permanently into the box-section.

In order to gain greater control over the application of load the hydraulic cabinet was switched to 'load-maintainer'. This facility allows load to be carefully applied with a hand-tensioned spring control. The sensitivity of the control meant that overstressing could be easily avoided; this is an important consideration in stress-relaxation tests. The displacement rate for the load-maintainer was constant at 0.3 mm/second.

The initial load in each rope was measured accurately using the loadcell connected to the Hewlett Packard data acquisition system. However, once the load had been transferred, readings were taken from two acoustic vibrating wire strain gauges fixed to opposite sides of

the box-section near the mid-point. This type of gauge was chosen since it is noted for its stability over long periods of time. The gauges were used to measure the change in strain in the steel box-section as the Parafil ropes relaxed; the change in strain could be related to the change in load in the rope once the axial stiffness of the section was known.

The box-section stiffness (area $\times$ modulus) was determined from a series of control tests up to 400 kN load in which readings of load and strain were recorded. Three determinations of the stiffness were performed and an average from the three runs was used. The box-section stiffness was found to be more than seventy times larger than that of the Parafil rope itself. Hence, while it was possible to measure the change in strain in the box-section as the ropes relaxed, the effect of this change on the stress-relaxation behaviour of the rope was negligible owing to the relative stiffnesses of the box-section and the rope.

The vibrating wire gauge consists of a thin steel wire held in tension between two anchorages as shown in Figure 5.3. The wire may be set into transverse vibration through excitation from a pulse of current delivered by an electromagnet. The gauge operates on the principle that a change in the length of a wire under tension produces a change in its frequency; the frequency change can be measured and simply related to a change in strain. During the tests, the time periods (reciprocal frequencies) of the gauges were measured directly by timing a count of one hundred cycles using a period counter manufactured by Gage Technique Ltd. The scatter in the five-figure digit emitted by the counter was typically ± 1 ; for the 5.5 inch gauge length adopted and an operating frequency of around 800 Hz, this scatter corresponds to a practical measuring resolution of 1.5 microstrain. In turn, this resolution leads to an accuracy in the results for load (in the rope) of about $\pm 0.3\%$ NBL.

Several trial tests were performed to assess whether there was any loss of load upon stress transfer. Readings were taken from the vibrating wire gauges prior to and after 'locking' the stress into the box-section; in each case the readings were the same. As expected, threaded connections produce a very positive system in which 'draw-in' cannot occur.

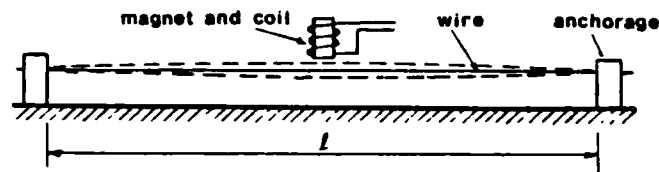


Figure 5.3 Acoustic vibrating wire strain gauge
(Window and Holister 1982).

At the end of each relaxation test the residual load in the rope was recorded accurately with the loadcell. To determine the residual load pressure was applied to the actuator until a displacement transducer indicated that the rope was being subjected to increased strain (and hence, load); the load recorded prior to the strain increase was taken as the residual load. In general, there was good agreement between the loadcell and the vibrating wire results. In cases where there was a discrepancy between the two final values for relaxation the complete set of vibrating wire readings was factored proportionally so that the final loss of load was that indicated by the more accurate loadcell. The effect of this on the intrinsic shape of the relaxation curves was thought to be negligible.

Measurement of the spike displacement within the dead-end terminal was monitored throughout the relaxation tests. For the first few hours, when the rate of movement was highest, a displacement transducer connected to the data logger was used. This was later replaced by a manually-read dial gauge.

At the end of each stress-relaxation test the residual strength of the rope was determined. The remaining stress in the rope was first removed and then, shortly afterwards, a tension test was performed. The same test procedure as that described in Chapter 4 was followed.

5.5 Results and discussion

Ten stress relaxation tests on 60 tonne Type G Parafil ropes were performed. Two tests were carried out at each of five different stress levels ranging from 30% to 70% of the nominal breaking stress. Test results are shown in Table 5.2.

In Tests 3 and 4 only, one or two yarns could be heard to fail just prior to 'locking' the load into the box-section. These tests were conducted at 70% NBL and 60% NBL respectively.

The relaxation behaviour for Tests 1 and 2 is shown in Figure 5.4; these were conducted at 70% NBL and 50% NBL respectively and were of 1000 hours duration. The graphs show that the majority of relaxation occurs within the first few hours. In fact, ninety per cent of the final 1000 hour stress-relaxation occurred within the first 100 hours. There was no measureable additional relaxation after the first 500 hours of either test.

A plot of the bed-down of the spike within the dead-end terminal for Test 1 (70% NBL) is shown in Figure 5.5. Account was taken of this bed-down when drawing the relaxation curve for this test (Figure 5.4). During Test 2 (50% NBL), however, negligible bed-down occurred. Both specimens had been pretensioned to 60% NBL prior to testing and it became apparent that bed-down effects could be ignored as long as the ropes were pretensioned to a load higher than that to which they would later be subjected. Hence, in subsequent tests the procedure of applying a pretensioning stress slightly larger than the initial test stress (but with a minimum of 60% NBL) was adopted.

The results of Tests 1 and 2 showed that little further information would be gained from allowing tests to run for longer than 100 hours. Hence, the duration of subsequent tests was reduced substantially; a duration of 100 hours was taken as the minimum although some tests ran for considerably longer periods.

A full set of stress-relaxation curves is shown in Figure 5.6. The curves, which have the same intrinsic shape are characterised by a rapid reduction in stress (which, to some extent, appears to be

Test number	Pretensioning limit (% NBL*)	Initial load (kN)	Initial load as % NBL	Relaxation after 100 hours(%NBL)	Test duration (hours)	Relaxation at end of test(%NBL)
1**	60	415.0	70.5	4.7	1000	5.2
3	73	415.5	70.6	4.2	166	4.6
4	63	355.6	60.4	4.2	118	4.2
5	63	355.1	60.4	4.9	143	5.1
2	60	294.0	50.0	3.6	1000	3.9
6	60	293.5	49.9	3.9	163	4.1
7	60	234.0	39.8	3.7	288	4.1
8	60	236.8	40.2	3.4	118	3.5
9	60	176.9	30.1	3.1	140	3.2
10	60	173.6	29.5	2.7	144	2.8

* NBL, nominal breaking load = 588.4 KN (60 tonnes)

** results adjusted for bed-down in fittings

Table 5.2 Stress-relaxation results for Parafil Type G ropes.

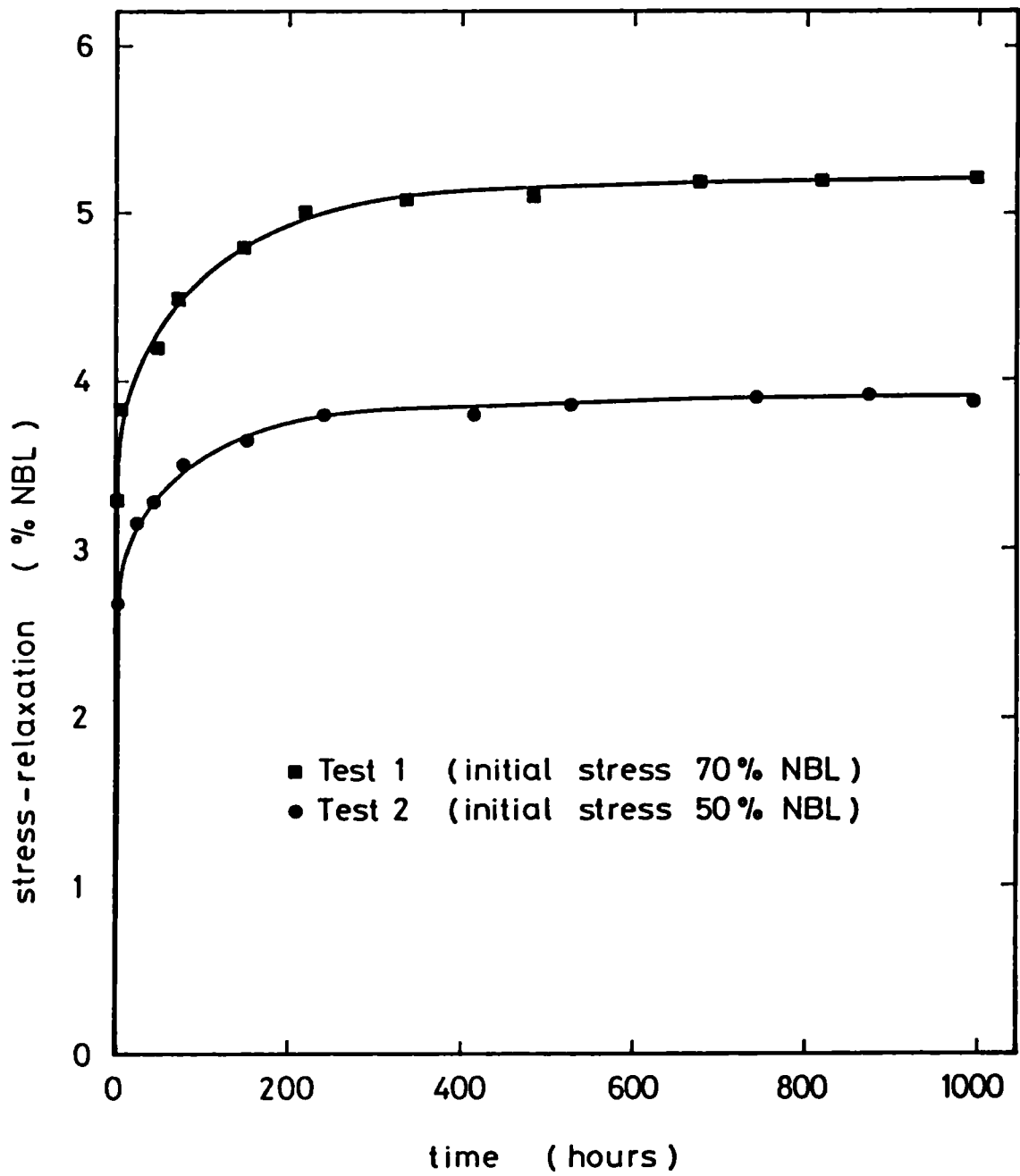


Figure 5.4 1000 hour stress-relaxation behaviour for initial loads of 70% and 50% NBL.

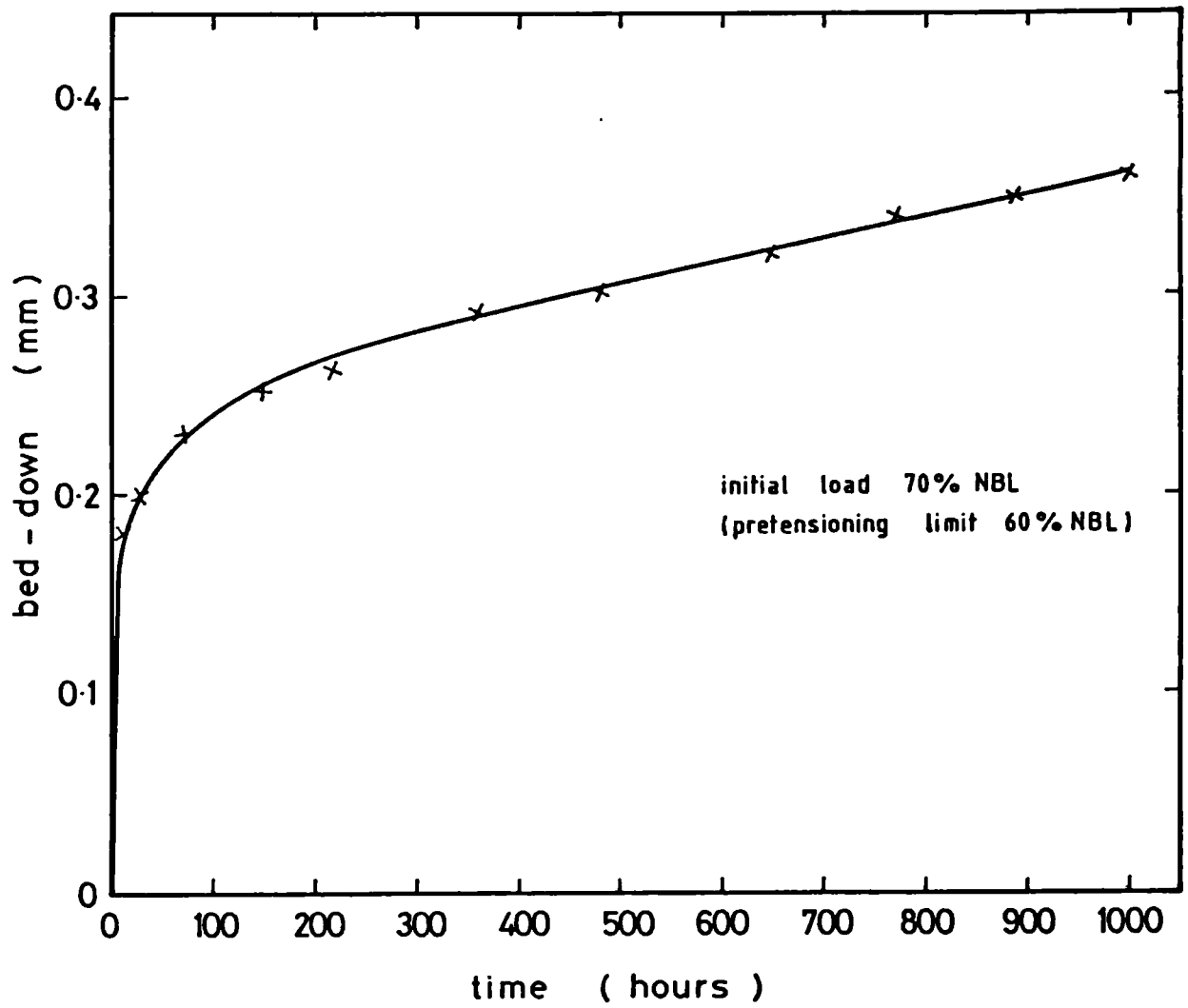


Figure 5.5 Bed-down within a single fitting for Test 1.

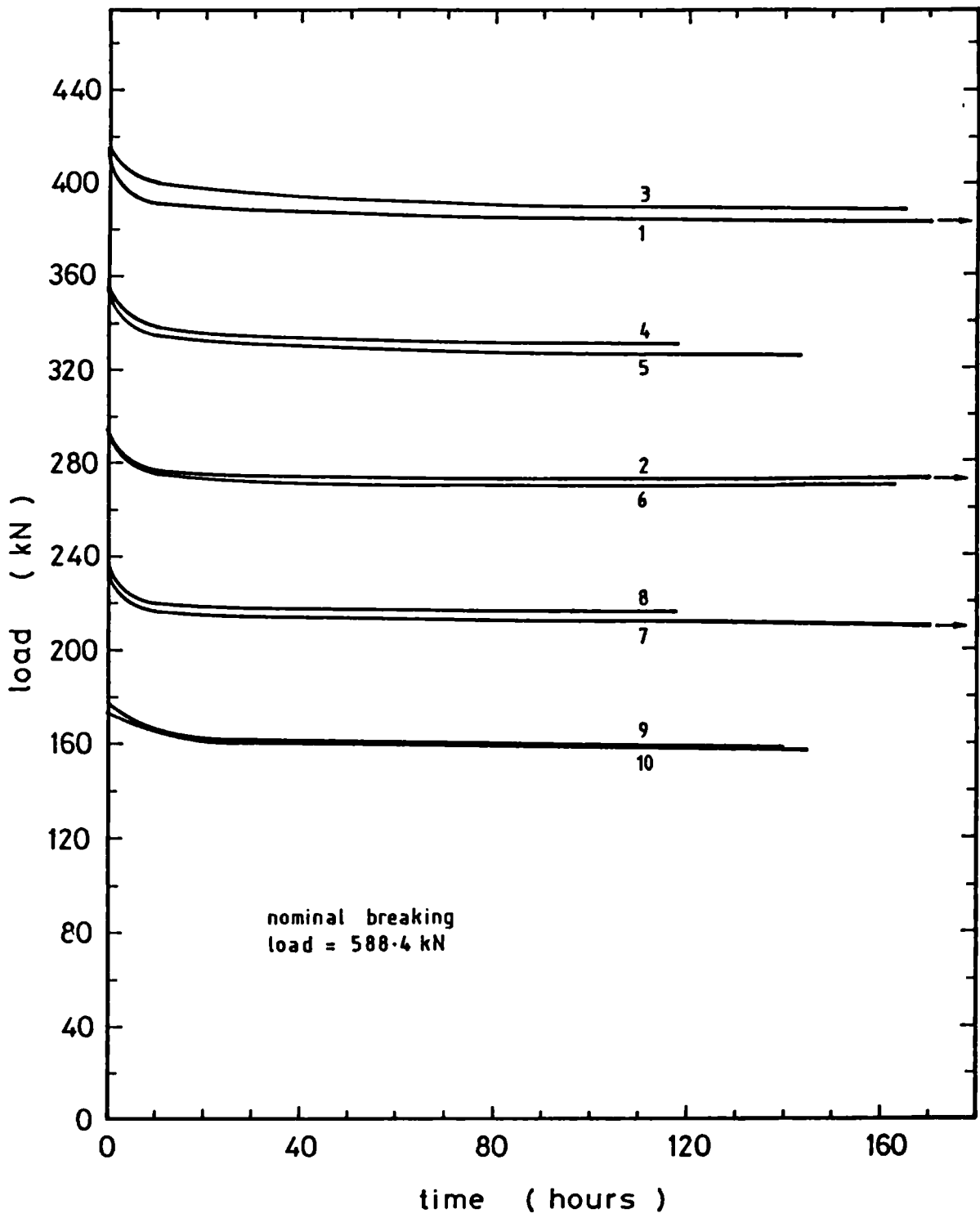


Figure 5.6 Stress-relaxation curves.

dependent on the applied stress) followed by an extremely gradual long-term stress-relaxation. The curves indicate a reasonable duplication of behaviour for tests conducted at the same stress levels.

5.6 Approximate estimation of long-term stress-relaxation

Figure 5.7 shows a plot of the stress-relaxation that has occurred within the first 100 hours of testing against the magnitude of the initial stress. The dependence of the 100-hour relaxation on stress may be regarded as linear (but not directly proportional) and may be expressed by the approximate relationship:

$$r = 1.82 + (4.03 \times 10^{-2})f \quad 30 \leq f \leq 70 \quad 5.1$$

where: r is the 100 hour stress-relaxation expressed as a % NBL
 f is the initial stress expressed as a % NBL.

When test periods beyond 100 hours are considered the linear relationship between applied stress and relaxation becomes less tenable and so 100 hours is taken as a convenient time period marking the division between short-term and long-term stress-relaxation behaviour.

Figure 5.6 indicates that the rate of relaxation for time periods exceeding 100 hours is broadly similar, regardless of stress. (In agreement with this observation, Cook et al. (1982) were able to identify a dependence of the short-term stress-relaxation in Kevlar 49 yarns on the applied stress but could not do so for long-term relaxation). The rate of long-term relaxation is best determined from the results of the two 1000 hour tests for which sensible plots of stress-relaxation against the logarithm of time may be drawn. The plots (Figure 5.8) indicate that a linear relationship between these two parameters, as observed in tests on Kevlar 49 yarns and fibres (5.3), may also be interpreted for Parafil ropes. However, there is some indication, particularly from the results of Test 2, that the rate of relaxation perhaps decreases after the first few hundred hours; nevertheless, a 'best-fit' straight line lies completely within

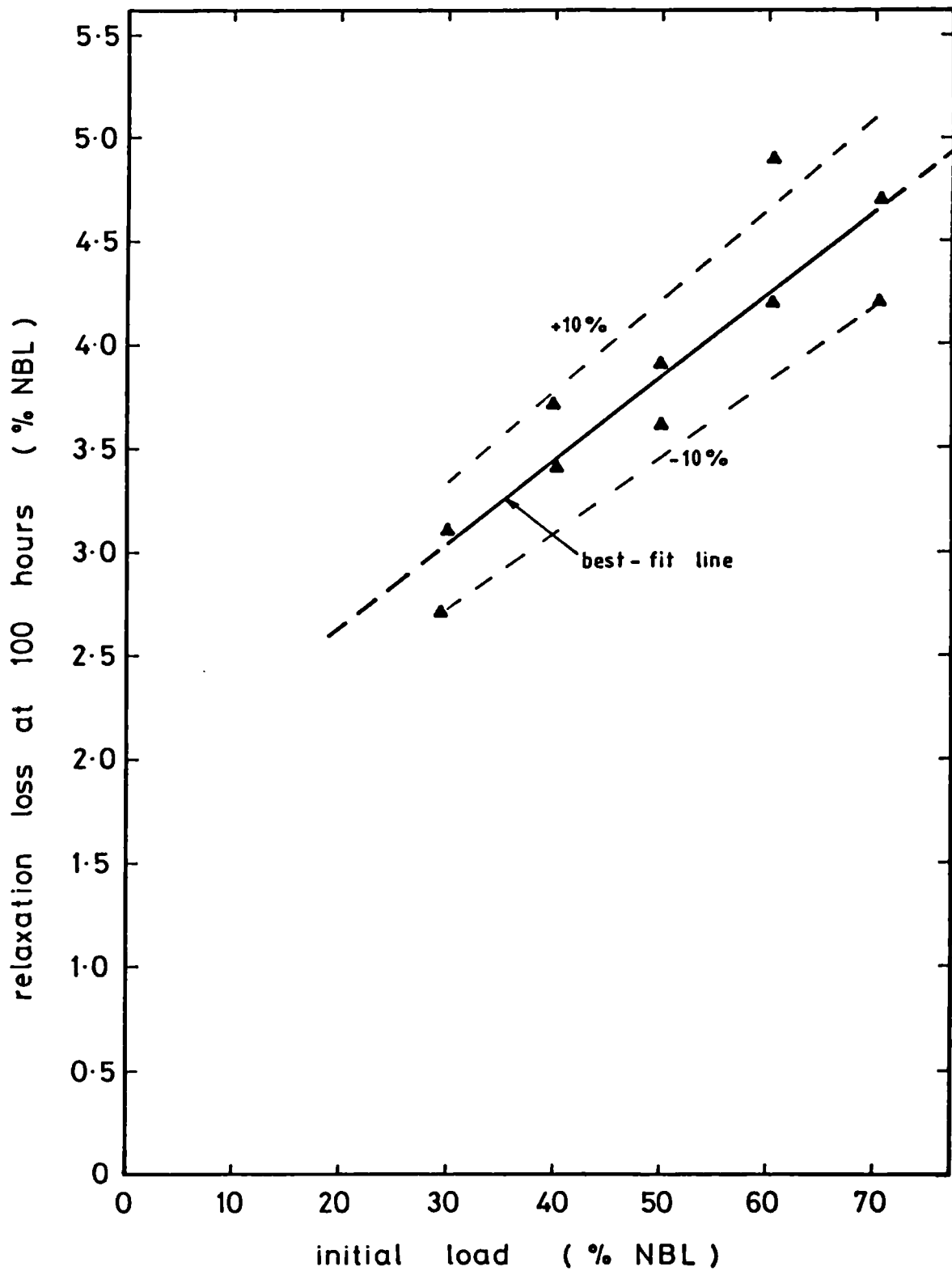


Figure 5.7 Dependency of 100 hour relaxation loss on initial stress.

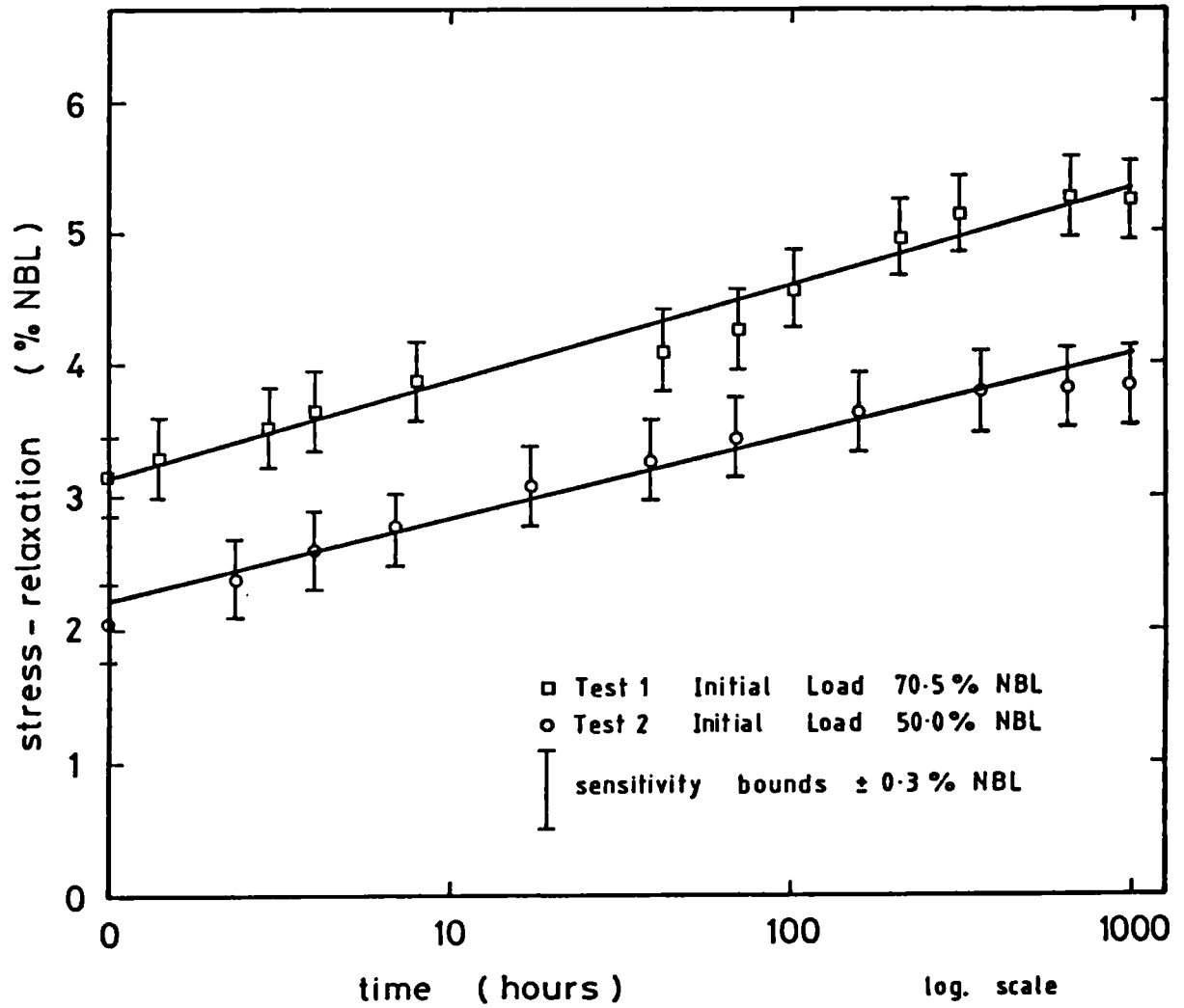


Figure 5.8 Stress-relaxation versus $\log_{10}$ time.

the sensitivity bounds of all the results. The rate of relaxation observed in these two tests was approximately equal, averaging 0.67% NBL per decade. Hence, if this rate of loss of stress with the logarithm of time is assumed to be stress-independent (for periods in excess of 100 hours) then the long-term stress-relaxation for Type G Parafil ropes may be predicted using the expression:

$$r = 1.82 + [4.03 \times 10^{-2} \times f] + [0.67 \log_{10} (t - 100)] \quad t > 100 \quad 5.2$$

where: r is the stress-relaxation expressed as % NBL
 f is the initial stress expressed as % NBL
 t is the time in hours.

Table 5.3 illustrates the 100 hour and 100 year relaxations that would be predicted from equations 5.1 and 5.2. One hundred years may be taken as a typical structural design life; for this time period the dependence of stress-relaxation on the initial stress (as predicted by Equation 5.2) becomes less apparent.

Nominal initial % NBL	Relaxation at 100 hours (% NBL)		Predicted relaxation at 100 years (%NBL) from eqn. 5.2
	from eqn. 5.1	average observed in tests	
30	3.0	2.9	7.0
40	3.4	3.5	7.4
50	3.8	3.8	7.8
60	4.2	4.5	8.2*
70	4.6	4.5	8.6*

* at these stresses, over long periods of time, Parafil may fail due to stress-rupture.

Table 5.3 Predicted and observed values for stress-relaxation.

5.7 Secondary relaxation

At the end of Test 2 (1000 hours) the residual load in the rope was restored to its original value of 50% of the nominal breaking load. The additional or secondary stress-relaxation was then monitored over the next 140 hours.

The complete loading history for this specimen is shown in Figure 5.9. An additional relaxation of 0.4% NBL had occurred at the end of the extra 140 hour test period; this represents only a 10% loss of the 'topping-up' stress which is less than might be expected.

The results of a test of this nature may be pertinent to an application in which it was essential to maintain a constant stress in the rope. For instance, this would be required if a Parafil rope were used as a tension member in an offshore tension leg platform; in this application it would be necessary to make periodic adjustments to the rope stress to account for any relaxation.

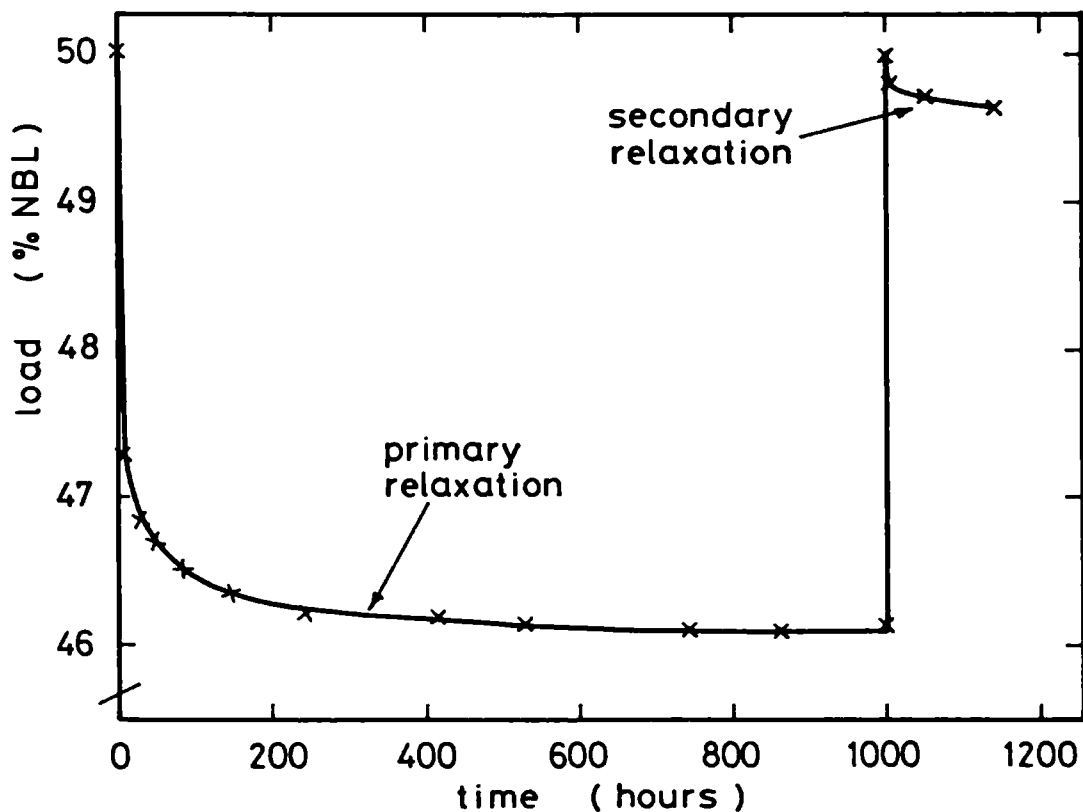


Figure 5.9 Primary and secondary stress-relaxation for Test 2 (50% NBL).

5.8 Strength-retention

At the end of each stress-relaxation test the ropes were destressed and tested in tension shortly afterwards. The test procedure was the same as that described in Chapter 4.

Test results are shown in Table 5.4. Generally the specimens had retained their full strength; the lowest strength obtained was equivalent to 90% of the nominal breaking load. Of the three ropes that displayed the lowest tensile strengths (Tests 4,7,9) there was some evidence that failure in two of them (Tests 4 and 7) had occurred close to an end terminal. However, no explanation can be offered for the large strength difference (17% NBL) observed between Tests 9 and 10, especially since both specimens had been subjected to very similar loading histories.

Test number	Initial load in relaxation test (%NBL)	Duration of relaxation test (hours)	Ultimate load (kN)	Ultimate stress (MPa)	Ultimate strain (%)	Overall modulus (GPa)	Strength retained (% NBL)
1	70.5	1000	588.8	1927	1.49	1.29	100.1
2	50.0	1000	638.3	2089	1.66	1.26	108.5
3	70.6	166	590.4	1932	1.55	1.25	100.3
4	60.4	118	529.4	1732	1.35	1.28	90.0
5	60.4	143	598.5	1958	1.58	1.24	101.7
6	49.9	163	592.6	1939	1.55	1.25	100.7
7	39.8	288	570.2	1866	1.52	1.23	96.9
8	40.2	118	578.0	1891	1.53	1.24	98.2
9	30.1	140	529.4	1732	1.42	1.22	90.0
10	29.5	144	632.4	2069	1.67	1.24	107.5

Table 5.4 Results of strength-retention tests.

For a material that displays a marked stress-rupture phenomenon lower residual strengths than those observed may have been expected. Other workers (e.g. Chiao et al. 1976, Christensen 1981), however, have also observed high residual strengths in Kevlar 49 specimens which have been subjected to long-term loads. These observations and the theories governing strength-retention are discussed in Chapter 7.

During the strength-retention tests the ropes displayed a higher stiffness than those tested during the investigation of tensile properties (Chapter 4) although the general shape of the stress-strain curves and the modes of failure were similar. The average increase in the elastic modulus (taken as the ratio of stress to strain at ultimate) was six per cent. Figure 5.10 indicates the stress-strain behaviour that can be expected for a Parafil Type G rope after it has spent a considerable time under load.

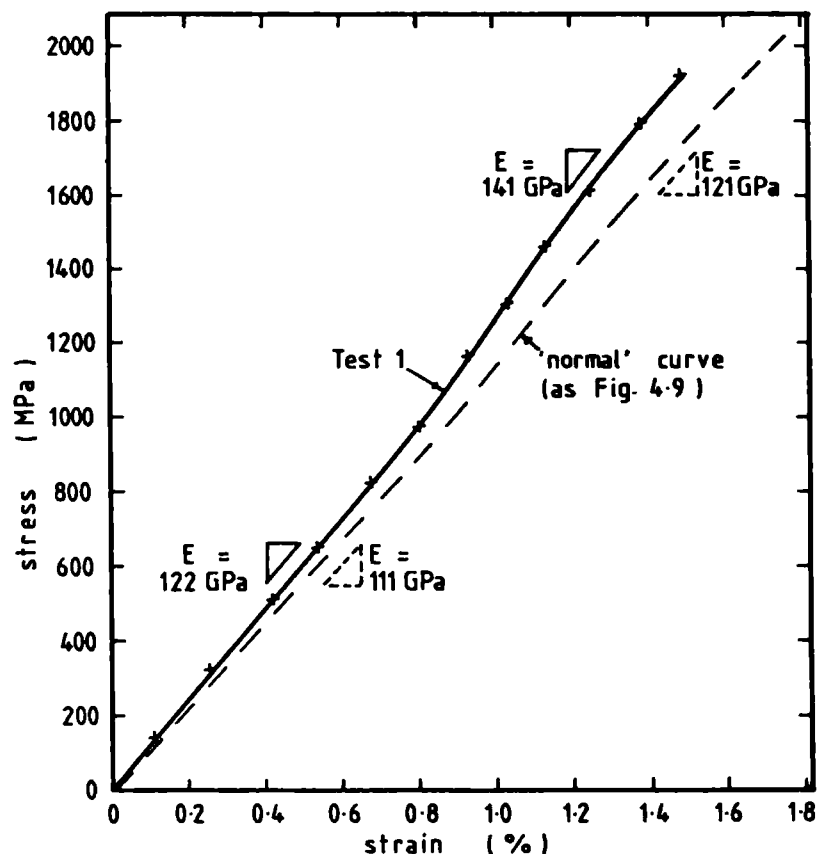
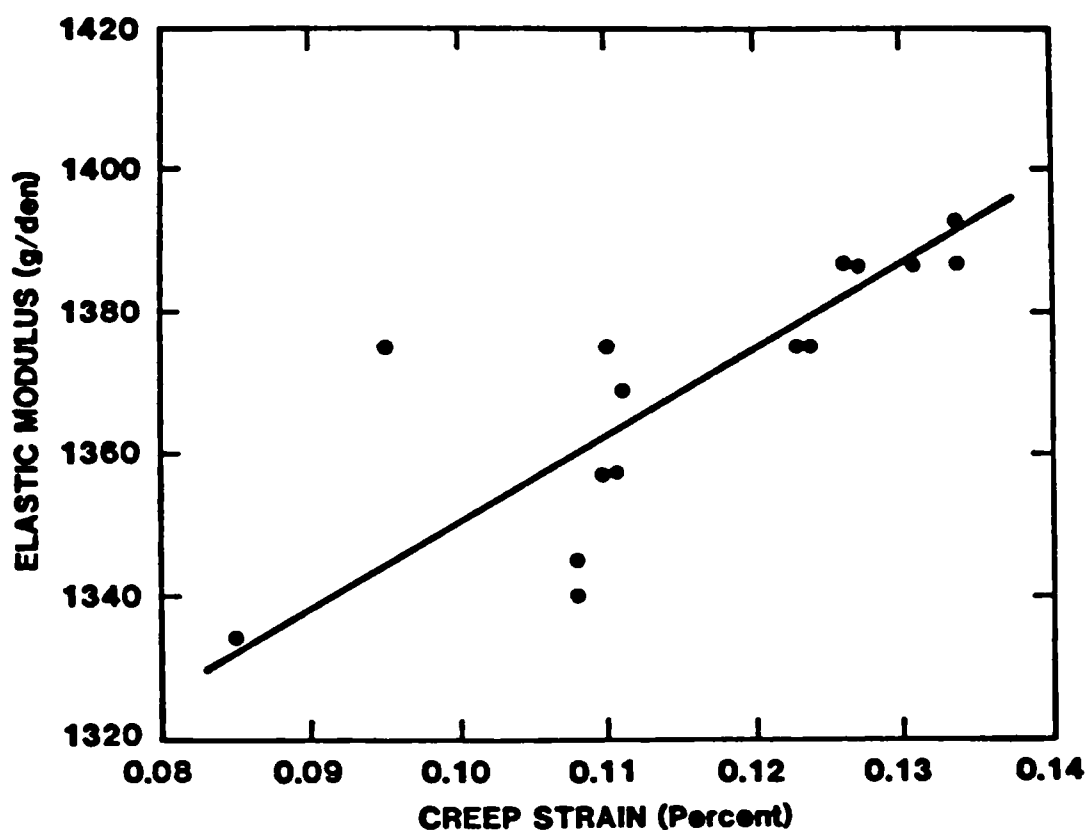


Figure 5.10 Stress-strain curve for Type G Parafil after spending 1000 hours under load (Test 1).

It is probable that the increase in the elastic modulus for Parafil ropes is time-dependent. For example, Ericksen (1984) found that the modulus of Kevlar 49 fibres increased progressively during a creep test as shown in Figure 5.11. An increase in the stiffness of about 6% had occurred after the initial 30 minutes of creep which implies a significantly greater rate of modulus increase than observed in Parafil ropes. Ericksen (1984) attributed the increasing fibre stiffness to crystallite rotation. Howard and Parratt (1985) have also observed that the stiffness of Kevlar increases with time under load; a 15% increase in the modulus of Kevlar 49 yarns was recorded after one year.



creep load = 24.6 g
 ultimate failure load = 42g
 modulus for straight tension test = 1018 g/den
 (g/den to GPa, multiply by 0.1271)

Figure 5.11 Modulus change with time as measured in a creep test on a Kevlar 49 fibre (Ericksen 1984).

5.9 Viscoelasticity

Ideally, experiments to monitor the decay of stress in a Parafil rope when subject to a decreasing strain should be performed since these would simulate the actual loading conditions for a rope when embedded in a prestressed concrete member. It is impracticable, however, to perform such experiments in a laboratory.

Nevertheless, one way in which the stress in a Parafil tendon, subject to a decreasing strain, might be determined would be to conduct two separate series of tests (ideally at nominally similar stress levels) to determine both the stress-relaxation and the creep behaviour of Parafil and then to try to relate the results using a rheological model. A full study of the viscoelastic behaviour of Parafil, however, was not an initial research objective; furthermore, Ko (1980) indicates that Kevlar 49 filaments exhibit non-linear viscoelastic behaviour, in which case it may not even be possible to formulate an adequate description for Parafil. Hence, the stress-relaxation tests described in this Chapter were conducted at likely levels of operating stress whereas the stress-rupture tests (Chapter 6 and 7), of which the recording of creep data was considered largely to be of secondary importance, were conducted at stress levels sufficiently high to ensure that failure occurred within sensible timescales.

However, an attempt was made to relate the results of a stress-relaxation test conducted with an initial stress equivalent to 70% of the nominal breaking stress (the highest stress level conducted in the series) to the results of a creep test in which the load was maintained at 70% NBL (the lowest stress level adopted in the test series on stress-rupture). A non-linear viscoelastic model as developed by Kumar and Gupta (1977, 1978) for describing the time-dependent behaviour of textile fibres was employed. The model, shown in Figure 5.12, includes a dashpot containing a non-Newtonian liquid, hence the non-linearity of the model.

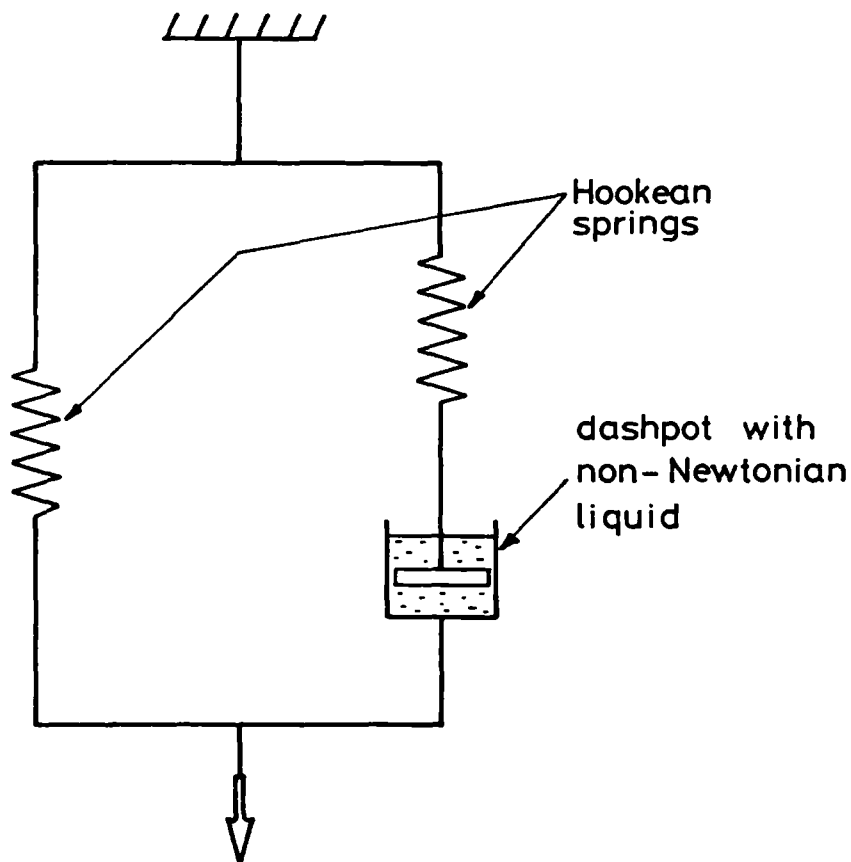


Figure 5.12 Non-linear viscoelastic model for textile fibres
(Kumar and Gupta 1978).

After several iterations of the required procedure suitable model constants were determined such that the creep curve was accurately regenerated; however, the model failed to accurately describe the corresponding stress-relaxation curve, or even the general dependence of relaxation on the applied stress (Figure 5.13).

Hence, the non-linear viscoelastic behaviour of Parafil Type G ropes remains an area in which extensive further research is required. For the time being, the procedure of treating the various causes of prestress loss as being independent (as largely adopted for steel tendons) will be retained for Parafil since there is presently insufficient understanding to pursue a more rigorous analysis.

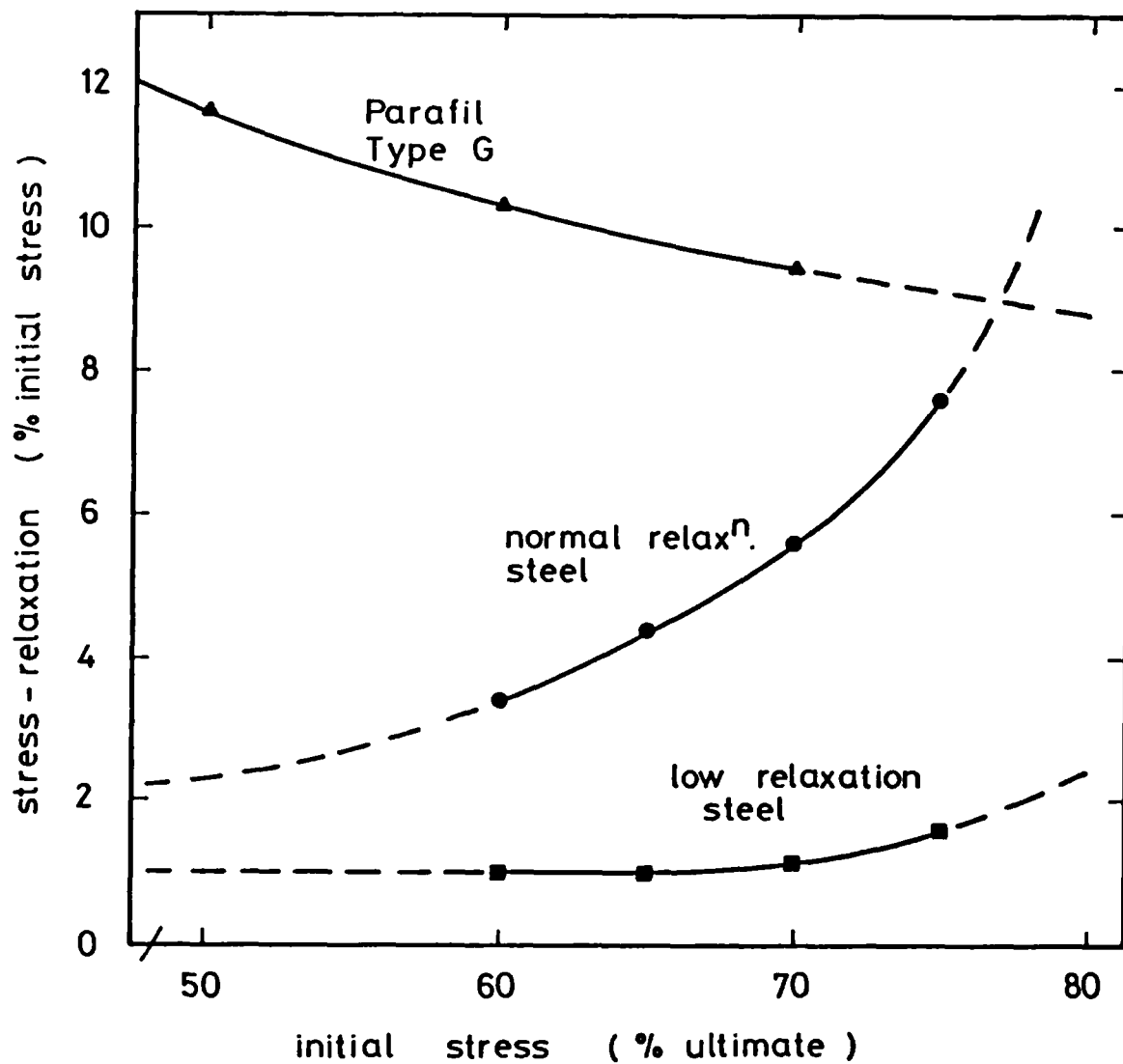
5.10 A comparison between the prestress losses in Parafil and steel tendons

The stress-relaxation displayed by Parafil Type G ropes is higher than for conventional steel products. For example, when an initial stress equal to 70% of the nominal breaking stress is applied, Parafil exhibits a loss of 7.5% of the initial stress (equivalent to 5.2% NBL) after 1000 hours whereas the corresponding figures for normal and low-relaxation steel strand are 6% and 2% respectively.

Figure 5.13 illustrates the dependence of stress-relaxation on the initial stress for Parafil and steel strand. Relaxation, quoted as a percentage of the initial stress, is seen to increase for Parafil as the initial stress decreases whereas the reverse trend is observed in steel. The different mode of behaviour has implications in terms of the amount of relaxation that should be taken into consideration when designing prestressed concrete structures.

For steel tendons, the 1000 hour stress-relaxation figure (determined in constant strain tests) gives a reasonable approximation to the long-term loss of prestress that may be attributed to relaxation when a steel tendon is embedded in a prestressed concrete member. A value for the relaxation of Parafil tendons at 1000 hours would be inappropriate for design purposes, however, since Parafil continues to display high stress-relaxation at low stresses. An estimate for the relaxation based on a time period equal to the proposed structural design life would be more relevant. Equation 5.2 may be used for this purpose.

While Parafil is likely to exhibit considerably more long-term stress-relaxation than steel, the lower elastic modulus of Parafil reduces significantly those losses due to elastic deformation, creep and shrinkage of the concrete. Also, in contrast to steel systems, the Parafil prestressing system does not cause any loss of prestress due to anchorage slip. Hence, when total prestress losses are considered, Parafil Type G tendons compare favourably with steel tendons. Table 5.5 shows a comparison between the prestress losses for the two materials assuming an initial tendon stress equal to 60% of the ultimate and a structural design life of fifty years. The values for



steel data from Bridon
Wire Ltd. trade lit.

Figure 5.13 Effect of initial stress on relaxation after 1000 hours for Parafil and steel strand.

the losses in steel tendons as shown in Table 5.5 are based on figures found in trade literature published by the Somerset Wire Company and apply to a typical prestressed concrete member. The figures for the losses in Parafil tendons due to elastic deformation, shrinkage and creep of the concrete have been determined by factoring the corresponding figures for steel tendons by the ratio of the elastic moduli.

Prestress loss	Parafil Type G	Steel strand	
		normal relaxation	low relaxation
Stress-relaxation	13.4	3.4	1.0
Elastic deformation	4.1	7	7
Shrinkage	2.4	4	4
Creep	4.1	7	7
Anchorage slip	<u>0</u>	<u>2</u>	<u>2</u>
Total	24.0	23.4	21.0

prestress losses as % initial stress
 initial stress assumed to be 60% of ultimate
 50 year structural design life assumed

Table 5.5 Comparison between typical prestress losses in Type G Parafil and steel strand.

CHAPTER 6: STRESS-RUPTURE AND CREEP: AN EXPERIMENTAL INVESTIGATION

6.1 Introduction

Although ultimate strength and modulus of elasticity are the most important short-term properties of any structural material they provide little information about the behaviour of a material when it is subjected to permanent loads. When some materials are exposed to stresses less than their initial ultimate stress for long periods of time, they eventually creep to failure. Hence, when determining suitable operating stresses for these materials questions of 'lifetime under stress' and creep deformation are much more pertinent.

It has long been recognised that the materials from which ropes are made, both natural and synthetic, are prone to stress-damage. For this reason it is common for the operating loads of ropes to be limited to a small percentage (10% to 25%) of their rated rope strength. In practice, visual checks for damage are normally undertaken and it is common for ropes to be withdrawn from service after a specified time, even if there are no obvious signs of deterioration.

The operating conditions of prestressing tendons present a particularly extreme example since, for structural reasons, tendons need to be permanently highly stressed. A deep understanding of the time-dependent nature of stress-rupture in Parafil ropes is clearly required, therefore, if full advantage is to be taken of their high inherent strength. Without this understanding it would be necessary to apply large factors of safety to the short-term strength of Parafil which would make its ability to compete with more traditional materials less effective. For instance, while the initial strength of Parafil exceeds that of steel tendons, the latter do not exhibit a stress-rupture phenomenon at ambient temperature (except when subject to stress-corrosion). The long-term behaviour of Parafil ropes, at high stress levels, is, therefore, of fundamental significance to their application in prestressed concrete.

This chapter presents the results from a series of constant load stress-rupture tests on 60 tonne Type G Parafil ropes. Stress-rupture data for most materials tends to be well scattered and so ideally an

extensive testing programme involving hundreds of specimens ought to have been undertaken to give a full understanding. However, from a moderate number of tests, it will be shown that it is possible to establish overall trends of stress-rupture behaviour on the basis of both lifetime and strain measurements. Having established these trends, advantage will later be taken (Chapter 7) of the various existing theoretical and practical approaches to stress-rupture to reliably predict the long-term behaviour of Parafil ropes under both static and time-varying loads.

6.2 Test procedure and instrumentation

The stress-rupture tests were performed within the steel box-section previously described (Chapter 4); the same method of stressing was used. All specimens were pretensioned to 60% of the nominal breaking load in the normal manner before stressing to the appropriate level. The creep load was held constant using the load-maintaining facility of the hydraulic cabinet (5.4); an automatic 'cut-out' device meant that the tests could be left running unattended.

Applied loads were measured using the box-section gauging (4.4.6); this was accurate to $\pm 1\%$ NBL. In later tests, a pressure transducer (accuracy ± 1 kN) located in the hydraulic circuit, was also employed.

Creep displacements and spike bed-down within the dead-end terminal were measured using displacement transducers (LVDT's). For tests of short duration the LVDT's were monitored using the Hewlett-Packard data-acquisition system; for longer tests the transducers were monitored through the analogue-to-digital converter of a BBC microcomputer. The use of the BBC microcomputer during the longer stress-rupture tests meant a small loss in logging accuracy but enabled the sophisticated Hewlett-Packard system to be more effectively used elsewhere in the laboratory. Hence, as a precaution during these tests, intermittent readings of the creep displacement were also taken with a dial gauge; these readings were found to be in good agreement with those from the LVDT monitored through the BBC microcomputer.

Using carefully written software the BBC microcomputer was programmed to take LVDT readings whenever the rate of rope creep suddenly increased (as commonly occurs when a yarn within the core snaps). In this manner it was possible to record creep strains during the tertiary creep phase just prior to failure (6.3.3) which was often characterised by a sudden increase in the creep rate.

A mains voltage filter was incorporated in the electrical circuit of the BBC microcomputer to eliminate voltage fluctuations in the mains supply and thus prevent the microcomputer from 'crashing' - a problem sometimes encountered when microcomputers are operated for long periods of time. The filter worked successfully as illustrated by a test in which the BBC system was used continuously for five months without any problems arising.

6.3 Results and discussion

6.3.1 Introduction

Sixteen stress-rupture tests at stresses ranging from 68% to 95% of the nominal breaking stress, and with failure times extending to five months, have been performed. General results are shown in Table 6.1.

6.3.2 Times to break

Stress-rupture lifetime data is commonly plotted using stress versus log. lifetime coordinates (theoretical justification for the adoption of these coordinates is provided in Chapter 7) since this choice often leads to a linear plot and hence facilitates extrapolation of the results. Figure 6.1 shows the Parafil results plotted in this manner and although a considerable degree of scatter is observed in the rope lifetimes (as expected), a linear trend between applied stress and the logarithm of the 'time to break' may be interpreted.

6.3.3 Creep strains

Figures 6.2 to 6.5 illustrate the creep behaviour of the ropes; generally, an initial region of high creep rate was displayed (primary creep) followed by an almost linear steady-state secondary creep

Test Number	Applied load (kN)	% NBL	Time to break (t_B)	Ln t_B (secs)	Total# strain (%)	Elastic strain (%)	Final# creep strain (%)	Additional## bed-down (mm)
1	531	90	2hrs 15 min	9.00	1.69	1.52	0.17	1.38
2	560	95	immediate	-	1.64	1.64	0	0
3	501	85	27 min	7.37	1.58	1.48	0.10	0.92
4	469	79.5	2hrs 0 min	8.88	1.50	1.36	0.14	1.16
5	459	78	3hrs 31 min	9.44	failed inside terminal			
6*	449	76	11dys 22hrs 57min	13.85	1.67	-	-	1.94
7	503	85.5	22hrs 35min	11.31	1.66	1.48	0.18	1.46
8	507	86	2hrs 41min	9.18	1.63	1.48	0.15	1.52
9	532	90.5	1hr 3min	8.24	1.73	1.60	0.13	1.04
10	548	93	10 min	6.40	1.72	1.64	0.08	0.58
11	441	75	4hrs 25min	9.68	failed adjacent to terminal			
12	470	80	18hrs 56min	11.13	1.49	1.40	0.09	1.32
13	441	75	11dys 3hrs 55min	13.78	1.47	1.31	0.16	1.56
14	411	70	power failure	-	-	-	-	-
15	456	77.5	17hrs 10min	11.03	1.47	1.35	0.12	1.18
16**	412	68	155dys 3hrs 42min	16.41	1.41	1.23	0.18	5.10

* conducted at two load levels - equivalent load quoted

** subject to intermittent overstress of 4% NBL during first two hours

based on last recorded reading before failure

total from both terminals

Table 6.1 Stress-rupture results.

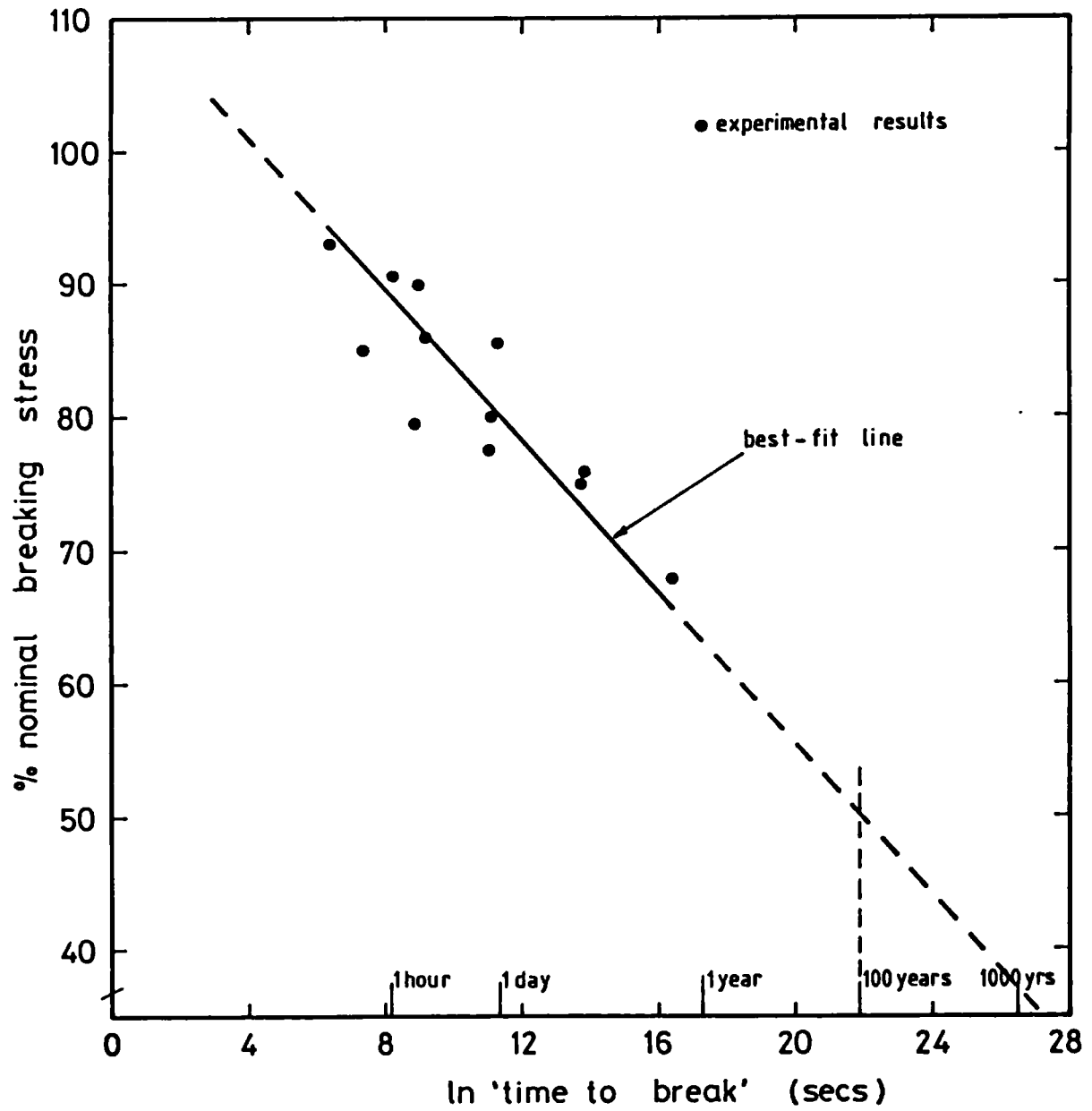


Figure 6.1 Dependence of lifetime on stress for Parafil.

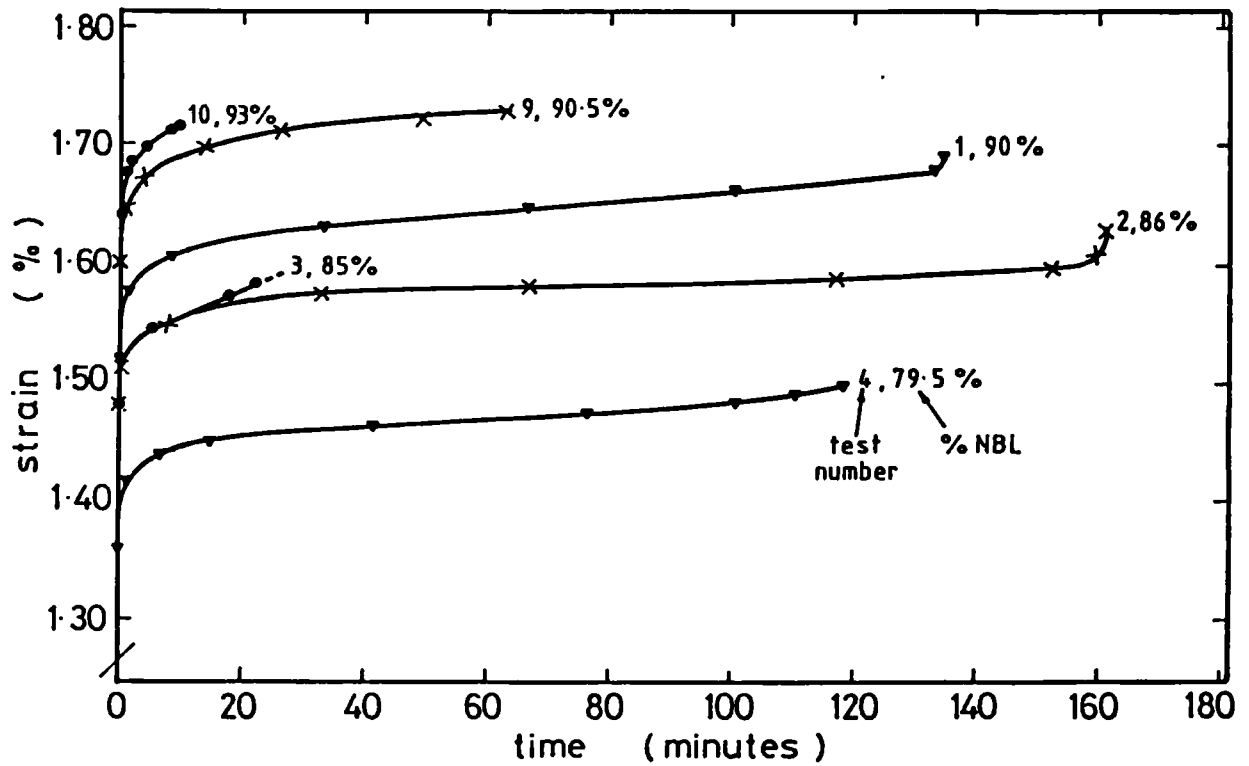


Figure 6.2 Creep behaviour.

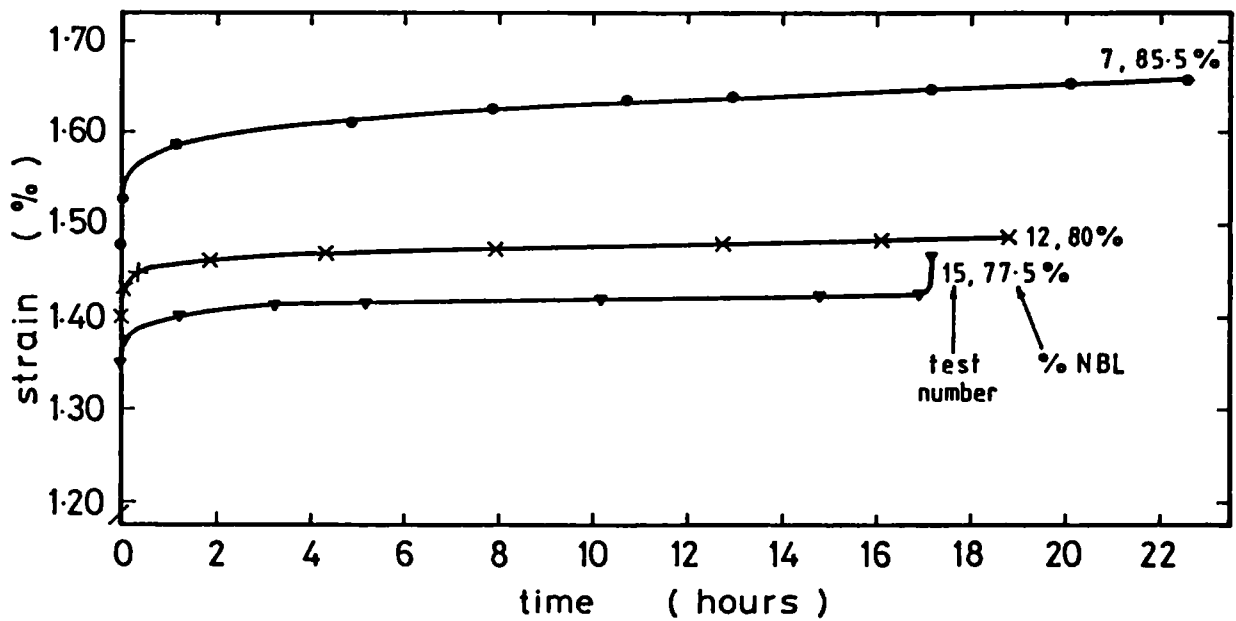


Figure 6.3 Creep behaviour.

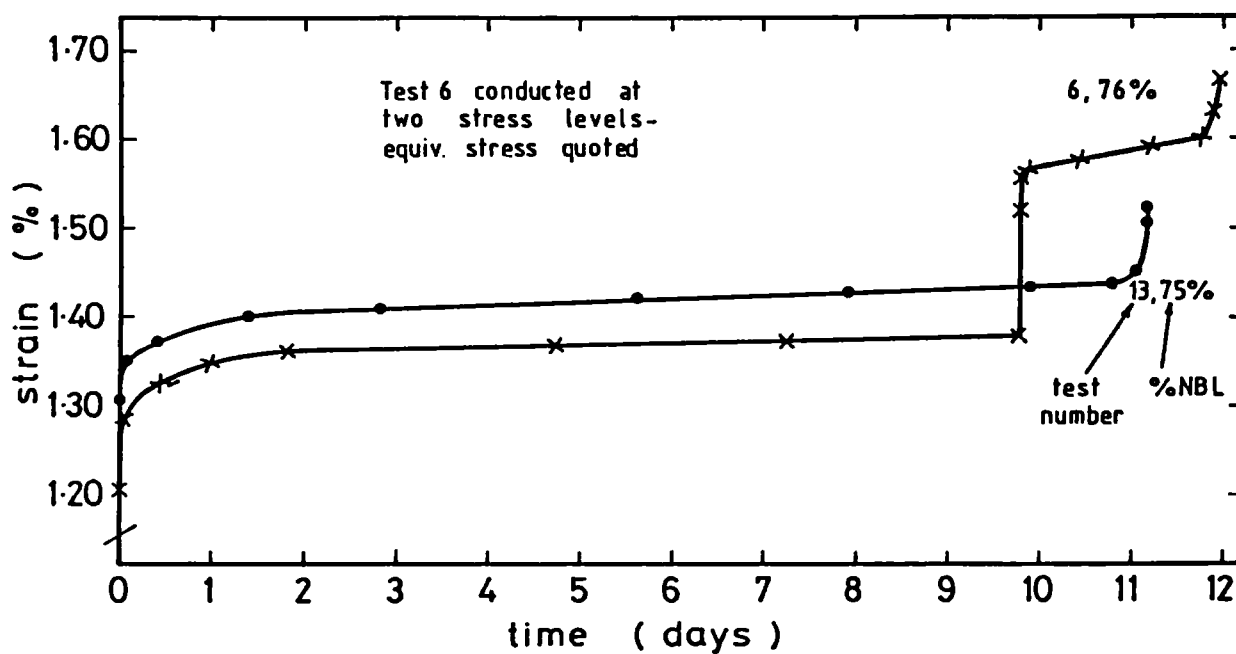


Figure 6.4 Creep behaviour.

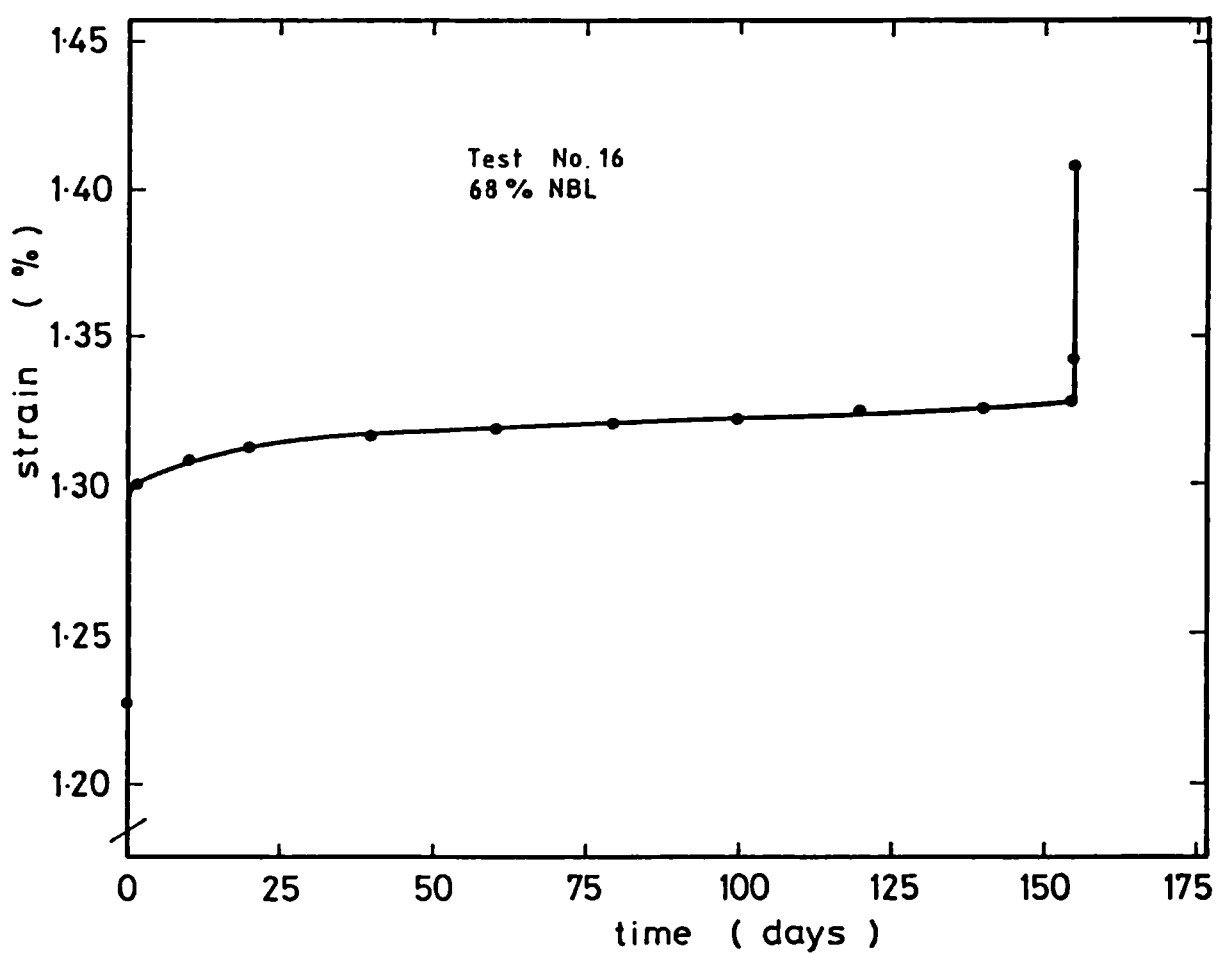


Figure 6.5 Creep behaviour.

region. The onset of failure was usually characterised by a dramatic rise in the creep rate which coincided with an increase in the frequency with which yarns could be heard to fail. The amount of creep strain associated with the sudden upturn in the creep curve (tertiary creep) prior to failure was found to be arbitrary and usually occurred over a small timescale. In some cases the sudden upturn was not discernible, on other occasions it was not observed owing to the policy of taking strain readings at fixed time intervals. Owing to this method of taking readings, the results for final creep strain (and total strain) shown in Table 6.1, should be regarded as approximate. Besides, of greater engineering value than the final creep strain is the magnitude of the strain marking the end of secondary creep and hence, the onset of failure; the dependence of this strain (ϵ_s) on the creep load is shown in Figure 6.6

When creep strains are plotted against the logarithm of time a linear dependence is quickly established (Figure 6.7). The dependence does not remain linear, however, and in all tests the rate of creep (with log. time) was found to increase as failure was approached. The initial strains recorded upon reaching the required stress level were found to be greater (by an average of 6%) than those expected on the basis of the average modulus observed in tension tests (Chapter 4). The increase was almost certainly due to the much slower loading rate associated with the load-maintaining facility of the hydraulic cabinet. The increased strains are also consistent with the observed high primary creep rates.

6.3.4 Mode of failure

In the majority of tests, yarns were heard to fail during load application prior to reaching the required stress level; the first yarn failures typically occurred at a load approximately equal to 70% of the nominal breaking load. It was also common for several yarns to fail just after the desired load level had been attained. Thereafter, yarn fracture would be intermittent or virtually unnoticeable until just prior to failure when the incidence of yarn failure would increase sharply.

Failure due to creep rupture was similar to that observed in the tension tests (4.5.3) except that the creep failures tended to be less

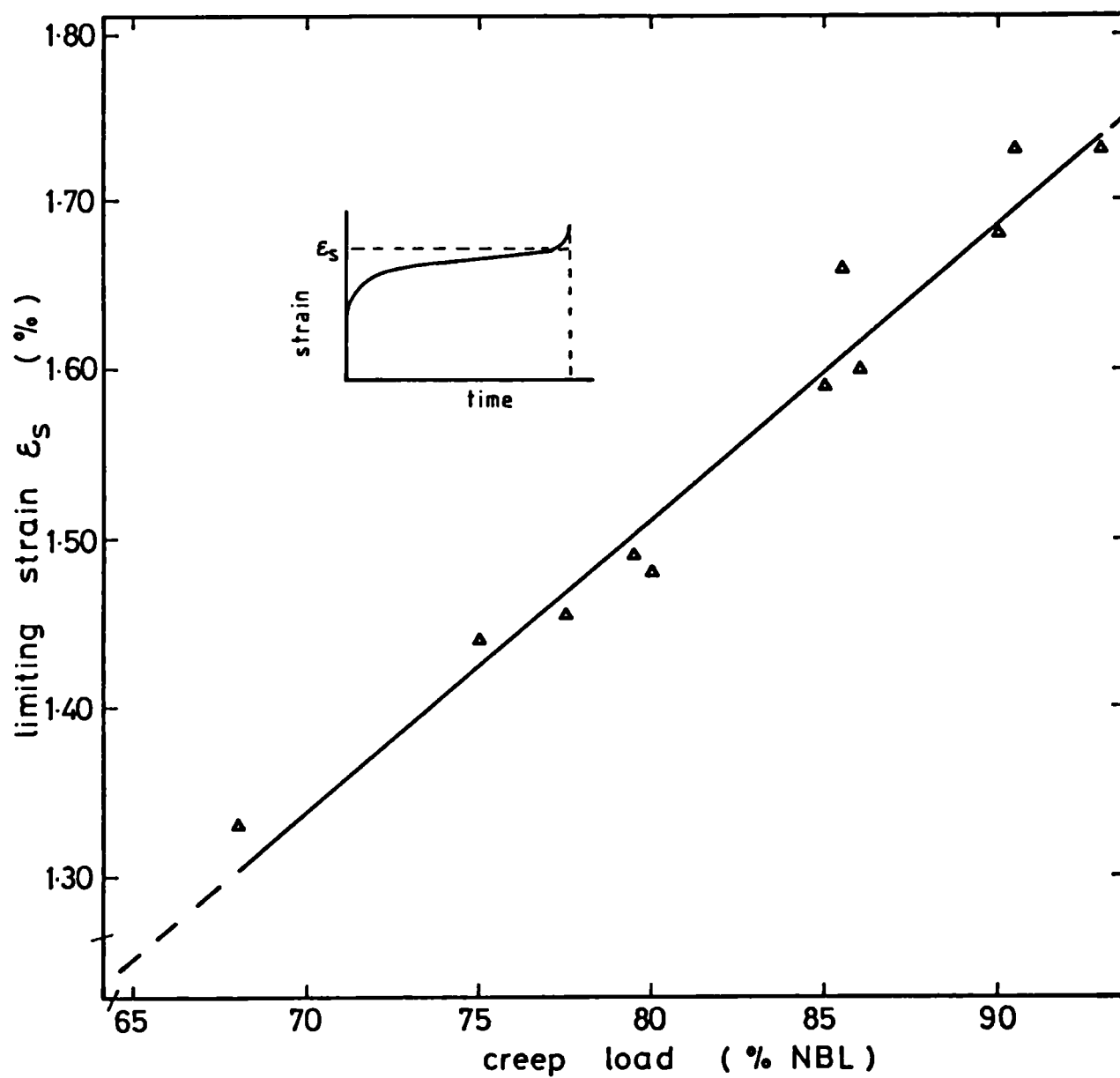


Figure 6.6 Dependence of limiting strain ϵ_s on creep load.

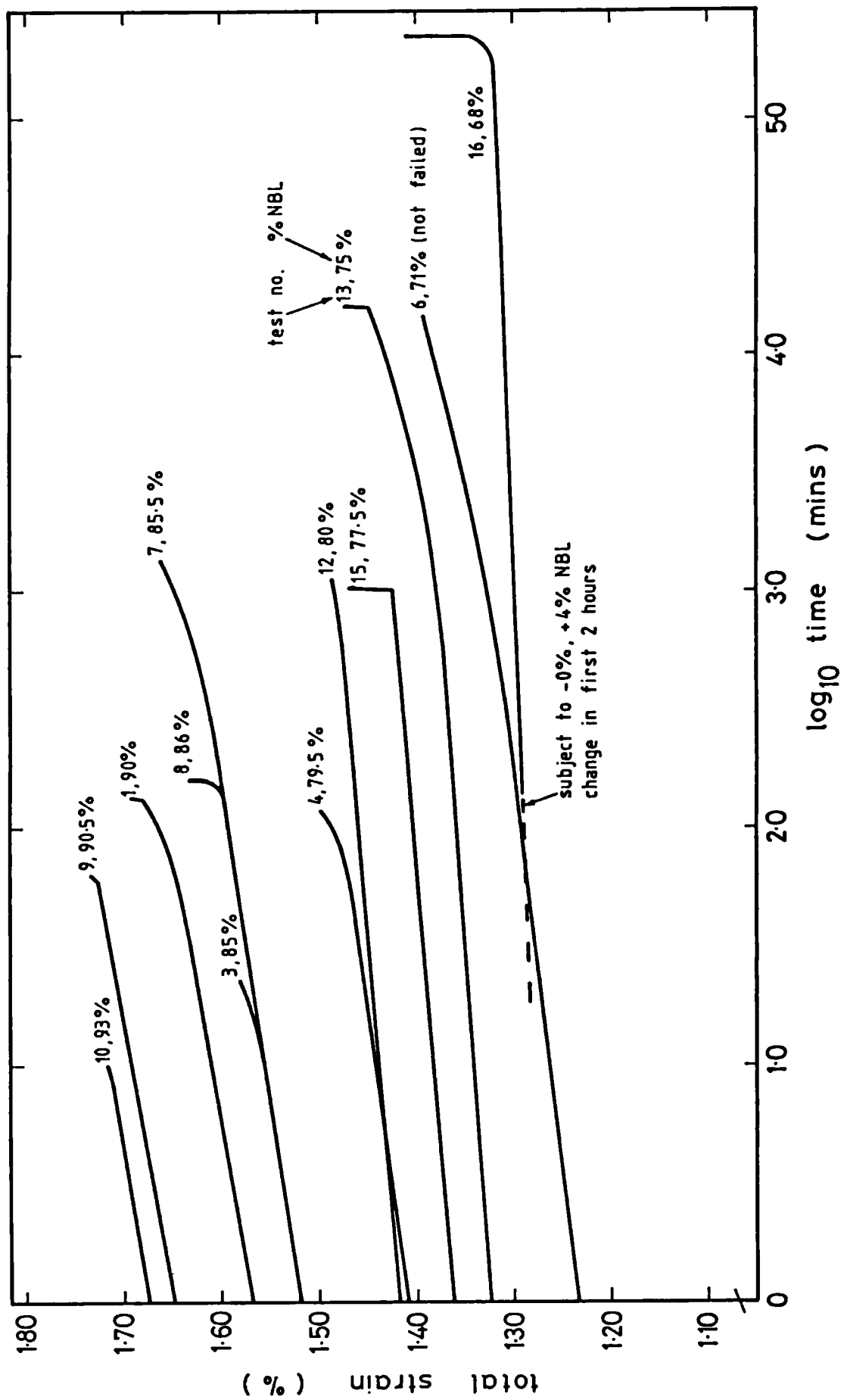


Figure 6.7 Creep strains versus log. time.

brittle. The slight difference in failure mode was probably due to the slower reloading rate associated with the load-maintainer. On some occasions failure was characterised by a series of reducing load steps. In other cases it was not possible to record the complete mode of failure owing to the intervention of the automatic cut-out device located in the hydraulic circuit.

6.3.5 Spike bed-down

The amount of additional bed-down that occurred within the rope terminals during the creep phase was small. Figure 6.8 indicates that the rate of bed-down diminishes quickly but does not reduce to zero. Indeed, during Test 16, which lasted five months, more than 5 mm of bed-down occurred (Table 6.1). Readings for spike bed-down within terminals were subtracted from the total displacement readings when determining creep strains.

6.4 Approximate empirical predictions of long-term behaviour under static loading.

6.4.1 From a consideration of 'times to break'

A 'best-fit' line for the experimental values of 'time to break' can be extrapolated so as to enable predictions of lifetimes at lower stress levels to be made (Figure 6.1). A linear relationship between stress and the logarithm of lifetime is consistent with various reaction rate theories for stress-rupture (Chapter 7). Hence, there is considerable theoretical justification for the extrapolation of this line. The equation for the 'best-fit' line, determined using the method of least squares, is found to be:

$$\ln t_B = 39.53 - 0.35 f \quad 68 \leq f \leq 93 \quad 6.1$$

where: t_B is the time to break in seconds

f is the static stress expressed as a percentage of the nominal breaking stress.

The result from Test 2 (at 95% NBL) in which the rope failed immediately upon reaching the required load and which would be classed

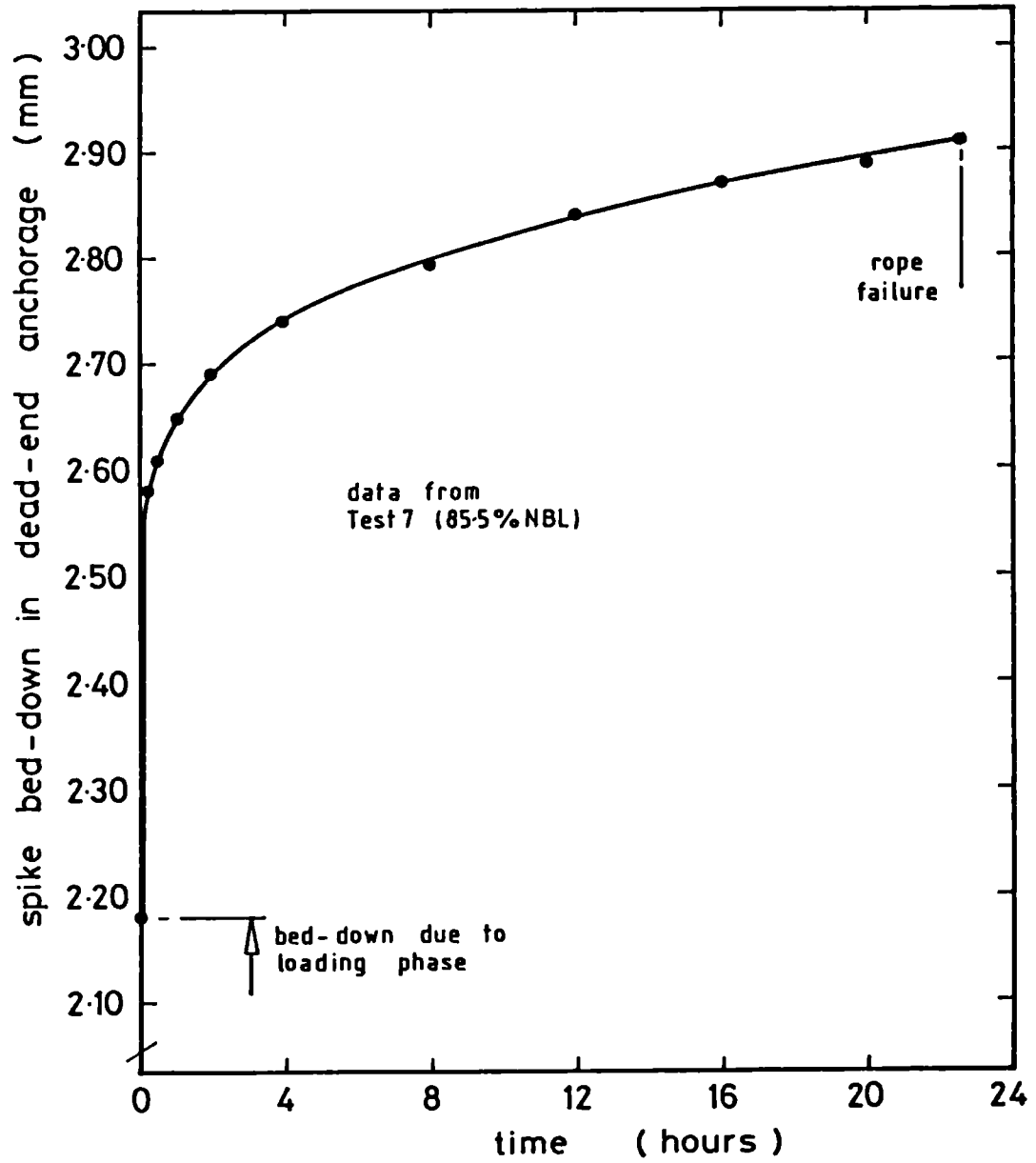


Figure 6.8 Spike bed-down within a single terminal during creep test No. 7.

as an outlier in Figure 6.1, has not been included in the derivation of Equation 6.1. At high stress levels, with low 'times to break', the time spent at intermediate stress levels during the loading process will lead to failure times which are not consistent with results obtained at lower stress levels (where the time spent on the loading phase is insignificant by comparison with the total duration of the test).

A typical structural design life is 100 years. The extrapolated 'best-fit' line for the experimental lifetime data (Figure 6.1) indicates that a load equal to 50% of the nominal breaking load would cause creep failure after this time period.

6.4.2 From a consideration of strains

6.4.2.1 Introduction

The measurement of creep strains during the stress-rupture tests was initially considered to be less important than the recording of 'times to break' since extrapolation of lifetime data enables predictions of rope lifetimes (outside the stress range investigated) to be made directly. However, as the test programme progressed, certain trends began to emerge between the measured creep strains and the magnitude of the applied stress, which provided the motivation for considering a method for making lifetime predictions (at stresses lower than those considered in the test programme) based on creep strain data. For the predictions based on strain data to have any credibility the method would, at the very least, have to yield good agreement with the experimentally observed stress-rupture lifetimes.

Three trends were observed in the stress-rupture tests:

- (1) The magnitude of the creep strain marking the end of the secondary creep phase and the onset of rope failure increased with the applied stress.
- (2) The strain at the end of the loading phase (i.e. the strain at the beginning of primary creep) increased as the applied stress increased (in accordance with the broadly linear stress-strain curve for Parafil).

(3) The rate of creep strain increased with stress.

Mathematical representations of these trends may be combined to enable alternative lifetime predictions for Parafil Type G ropes to be made.

6.4.2.2 Secondary creep strains

Figure 6.6 shows the dependence of the creep strain, ϵ_s , measured at the end of the secondary creep phase, on the applied stress. It can be inferred that the onset of creep failure is dependent on a limiting strain which increases linearly with stress.

Similarly, if the predominantly linear plots of strain versus log. time (Figure 6.7) are assumed to remain totally linear until failure occurs, i.e. any upward sweep in the creep curve is ignored, then an alternative limiting or failure strain, ϵ_f , may be defined. The dependence of the strain ϵ_f on the applied stress is also linear as illustrated in Figure 6.9; moreover, since the strain ϵ_f is extracted from plots of strain versus log. time (as opposed to absolute time) it is more suited to inclusion in a creep-strain method for making lifetime predictions.

When a linear regression analysis is performed on the data for the limiting strain ϵ_f and the corresponding values of applied stress (Figure 6.9) a coefficient of correlation of 0.98 is obtained. The equation of the 'best-fit' line is found to be:

$$\epsilon_f = 1.736 \times 10^{-2} f + 0.111 \quad 68 \leq f \leq 93 \quad 6.2$$

where: ϵ_f is the % limiting strain defined earlier
 f is the applied static stress expressed as a percentage
of the nominal breaking stress.

6.4.2.3 Initial strains

Since Parafil Type G ropes display broadly linear stress-strain behaviour then a linear relationship between the strain measured at the end of the loading phase (i.e. at the start of primary creep) and the applied stress would be expected. For mathematical convenience, however, the strain (ϵ_1) measured one minute after reaching the

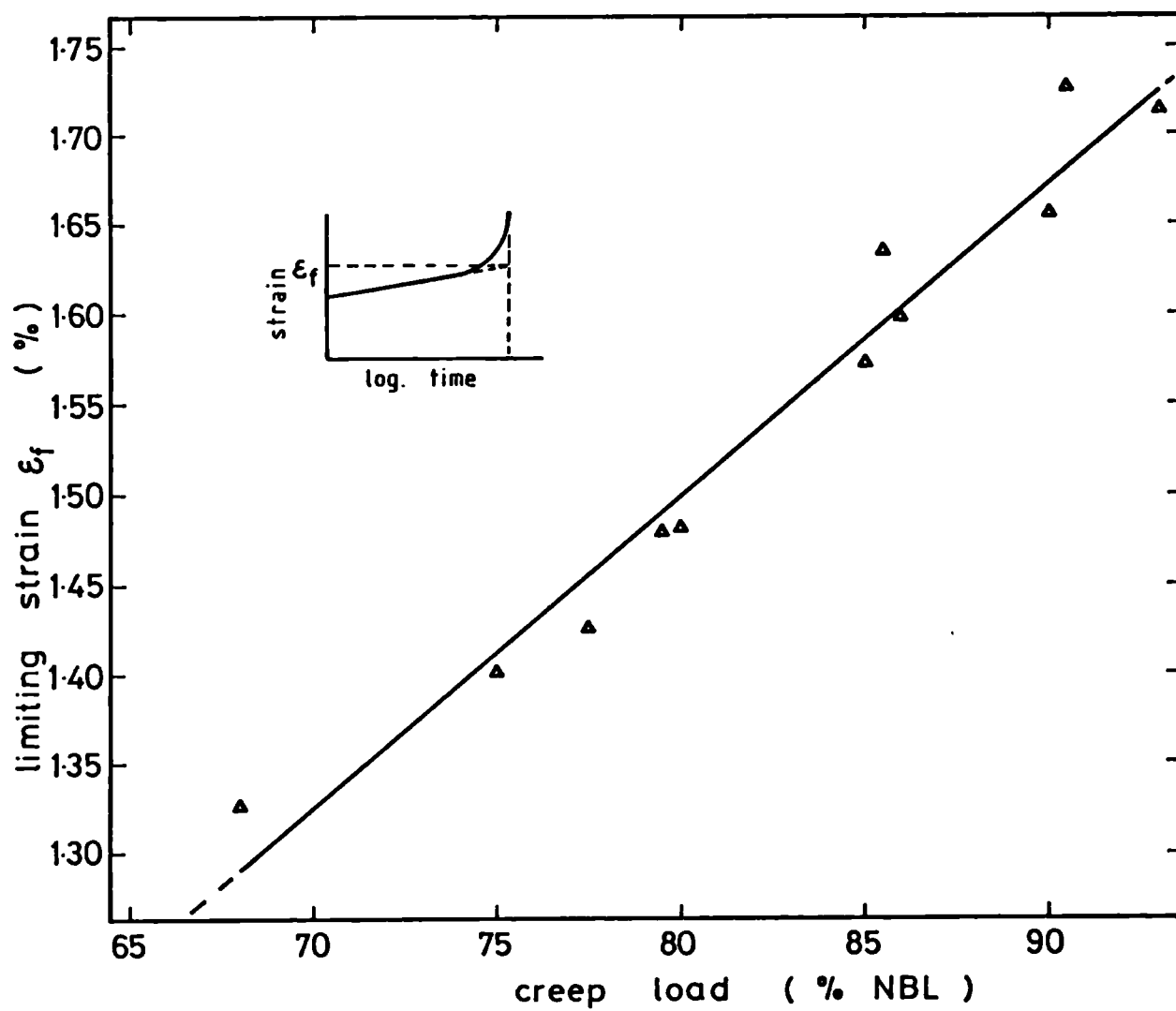


Figure 6.9 Effect of stress on limiting strain ϵ_f .

required stress level is taken as the initial strain; nevertheless, the linearity still holds as shown in Figure 6.10. The equation representing the 'best-fit' line for this data is found to be:

$$\epsilon_1 = 1.951 \times 10^{-2} f - 0.147 \quad 71 \leq f \leq 93 \quad 6.3$$

where: ϵ_1 is the % strain measured after one minute
 f is the applied stress expressed as a percentage of the nominal breaking stress.

Owing to a problem of maintaining a constant load during the first two hours of Test 16 the observed initial strain for this test was not included in the regression analysis. The coefficient of correlation for the remaining data was found to be 0.99; hence, Equation 6.3 represents the experimental data well.

6.4.2.4 Creep strain capacity

Having obtained expressions for the limiting strain ϵ_f (Equation 6.2) and the initial strain (Equation 6.3) the dependence on the applied stress of the creep strain capacity or the amount of creep that a rope is able to undergo before it ruptures ($\epsilon_f - \epsilon_1$) may be expressed as:

$$(\epsilon_f - \epsilon_1) = 0.258 - 0.215 \times 10^{-2} f \quad 6.4$$

Figure 6.11 indicates that the capacity for undergoing creep strain before failure increases as the applied stress decreases (intuitively this would be expected).

6.4.2.5 Creep rates with log. time

The creep strain capacity increases gradually as the applied stress decreases (Figure 6.11); this observation is only compatible with the expectation of much increased lifetimes at lower stresses if the rate of creep were to reduce significantly with stress. Figure 6.12 indicates that the rate of creep does indeed diminish rapidly as the applied static stress is reduced. Here, the creep rate is taken as the gradient of the linear portion of the creep strain versus log. time plots (Figure 6.7); this rate is consistent with the notion of an extrapolated limiting strain ϵ_f .

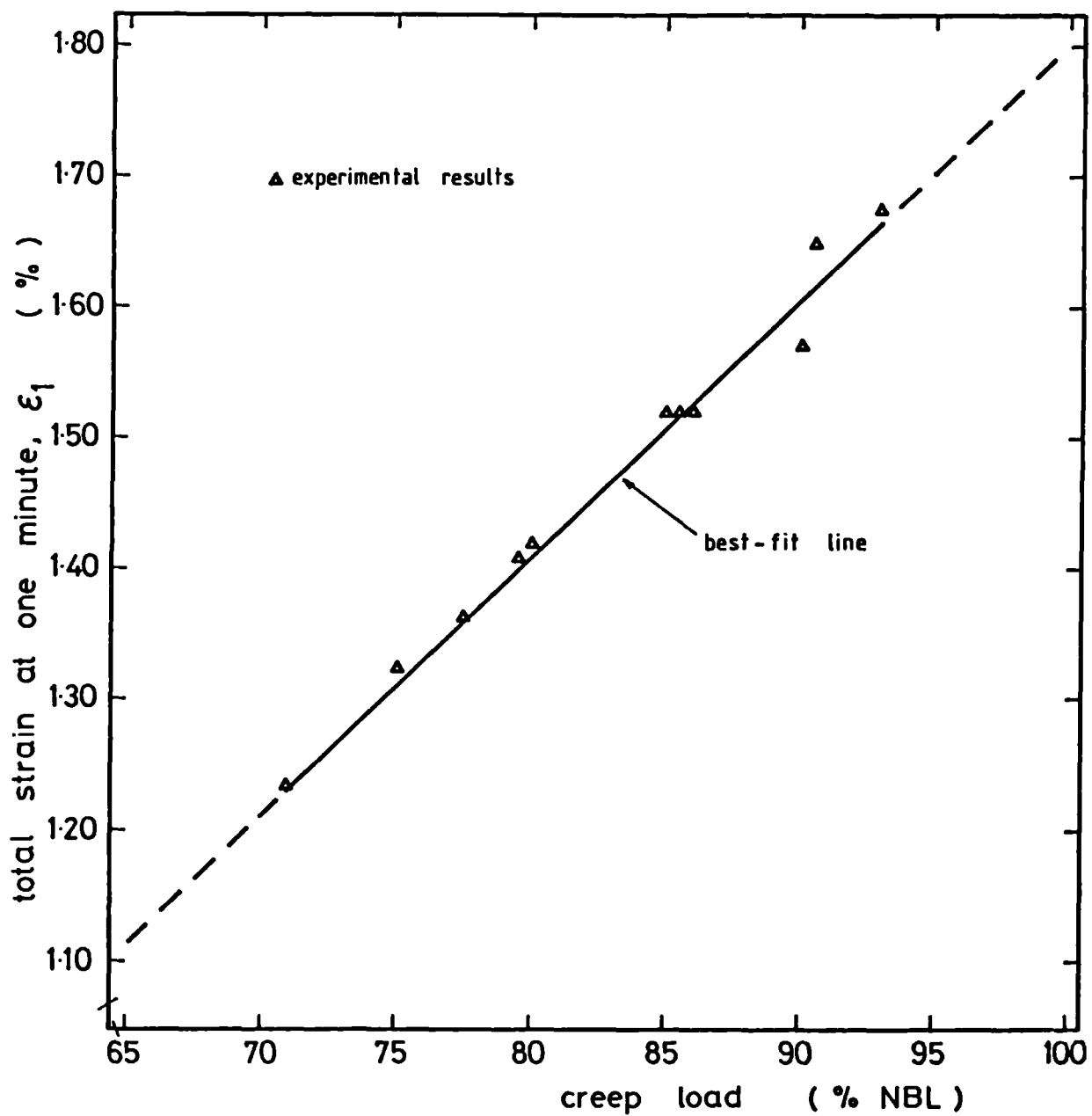


Figure 6.10 Effect of stress on strain at $t = 1$ minute.

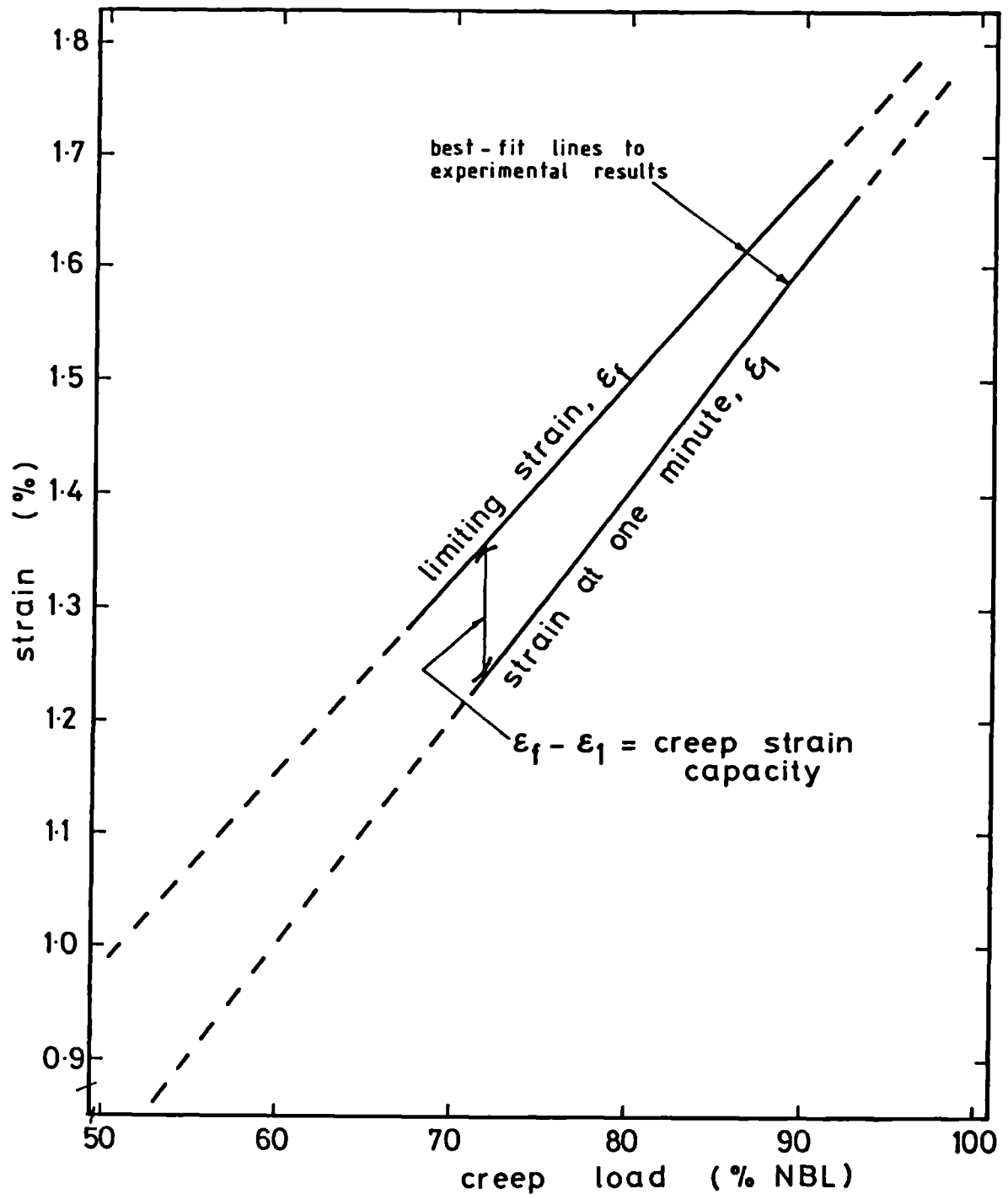


Figure 6.11 Effect of creep load on creep strain capacity.

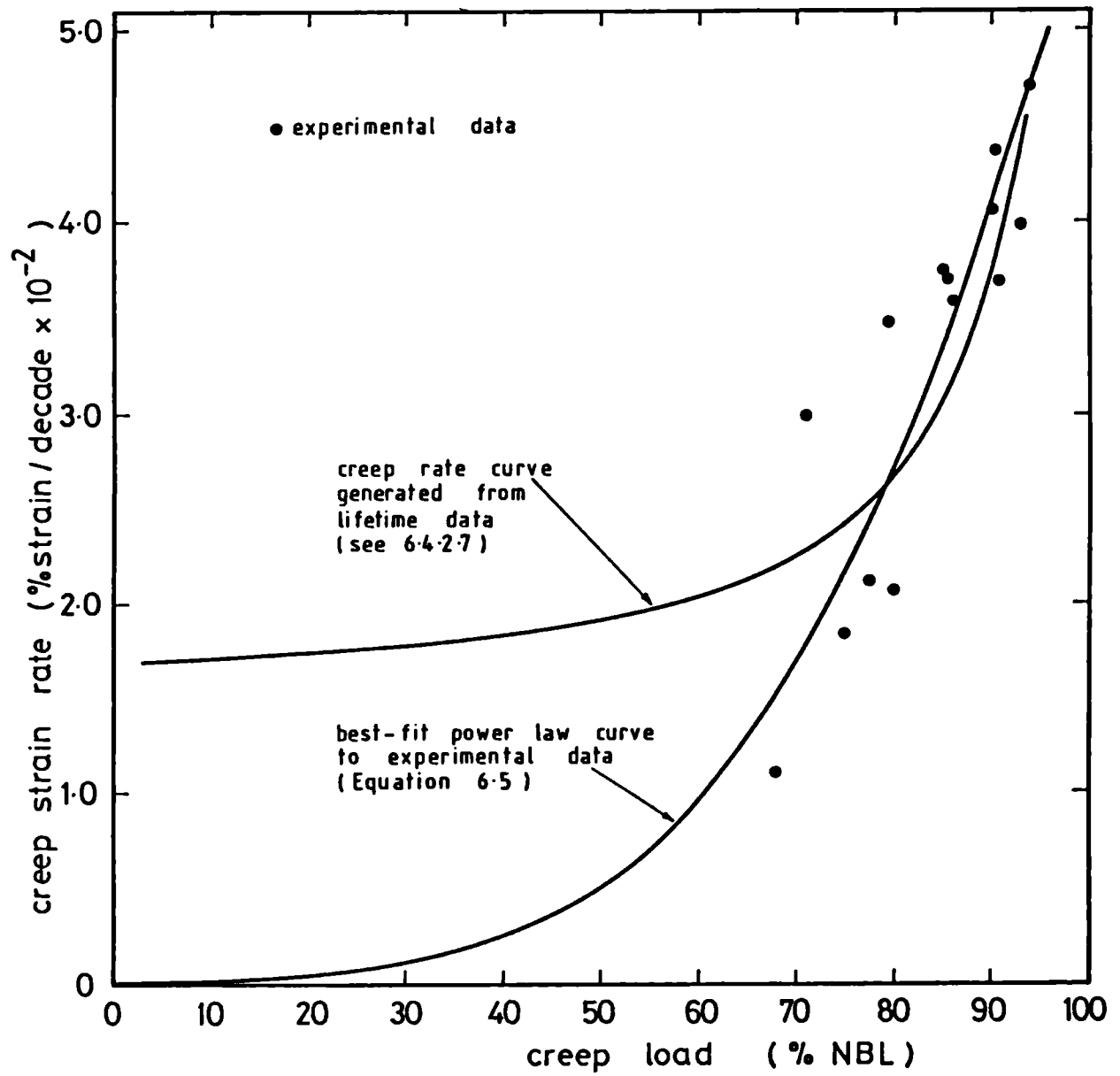


Figure 6.12 Effect of stress on creep strain rate.

To enable rational lifetime predictions to be made a power curve of the form $y = ax^b$ has been fitted to the creep rate data (using a standard curve fitting computer program); power curves of this form pass through the origin and so this choice seems realistic. The approximate relationship between the creep rate and the applied stress is given by:

$$r = (5.49 \times 10^{-9}) f^{3.52} \quad 68 \leq f \leq 93 \quad 6.5$$

where: r is the creep rate expressed as a % strain per decade
 (a decade is one increment on a base ten logarithmic scale)
 f is the applied stress expressed as a percentage of the nominal breaking stress.

Experimental values for the creep rate and the 'best-fit' power curve are shown in Figure 6.12.

6.4.2.6 Lifetime predictions

Stress-rupture lifetime predictions for Parafil Type G ropes based on experimentally recorded strain data may now be derived from a combination of the relationships defining the dependence of the creep strain capacity and the rate of creep on the applied stress (Equations 6.4, 6.5). Based on the observed rope behaviour and the assumptions made, the following equation is appropriate for predicting lifetimes:

$$\begin{aligned} \log_{10} t_B &= \frac{(\epsilon_f - \epsilon_1)}{r} \\ &= \frac{0.258 - (0.215 \times 10^{-2} f)}{(5.49 \times 10^{-9}) f^{3.52}} \end{aligned} \quad 6.6$$

where: t_B is the time to break in minutes
 f is the percentage of the nominal breaking stress.

Figure 6.13 illustrates the results of applying Equation 6.6 and provides a comparison with the experimental lifetime data. The lifetime predictions based on the strain criteria are in good agreement with the experimental lifetime data over the range of stress investigated. When stresses outside the test range are considered,

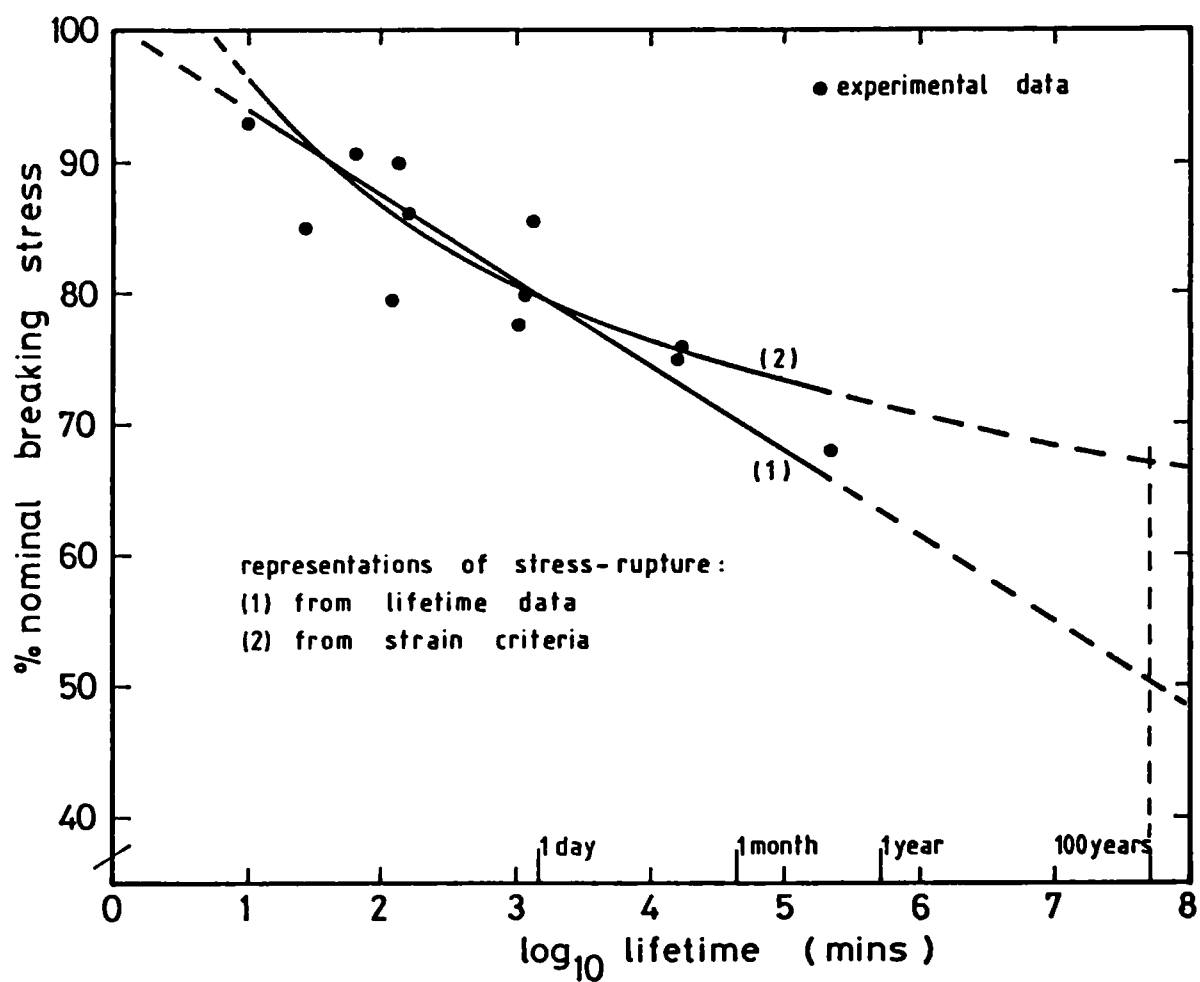


Figure 6.13 Predictions of long-term stress-rupture for Parafil from lifetime and strain data.

the two extrapolations shown in Figure 6.13 diverge, with the creep-strain method leading to predictions of much longer lifetimes at lower stresses than the direct extrapolation of the experimental lifetime results. The loads estimated to produce a creep failure after 100 years from a consideration of strains and from a consideration of lifetime data are 67% NBL and 50% NBL respectively.

6.4.2.7 Creep rates from lifetime data

It should be possible using Equation 6.6 to generate creep rate data which correspond to the experimental lifetime results. This can be achieved by substituting the equation representing the 'best-fit' line of the lifetime data (Equation 6.1) into Equation 6.6 and solving for the rate parameter r .

The results of following this procedure are shown in Figure 6.12. There is reasonable agreement between the 'generated' creep rate data and the experimentally recorded data over the range of stress investigated. This infers that the correlation between applied stress and creep strain capacity (Equation 6.4) is not simply fortuitous. Outside the investigated stress range however, (i.e. at lower stresses) the 'generated' creep rate curve diverges significantly from the experimental power law curve, levelling-off at around 0.018% creep strain per decade. The divergence is largely synonymous with the divergence of the two alternative lifetime curves shown in Figure 6.13.

Predictions of stress-rupture lifetimes at low stresses (based on strain criteria) are clearly very much dependent on the assumed correlation between creep rate and stress. If tests were conducted to measure creep rates at low stresses and the results agreed with the power law curve of Figure 6.12 then lifetime predictions based on strain criteria (i.e. curve 2 of Figure 6.13) would be favoured. If experimental creep rates at low stress levels were found to agree with the 'generated' creep rate curve of Figure 6.12, however, then this would strongly favour an extrapolation of the 'best-fit' line to the experimental lifetime data (i.e. curve 1 of Figure 6.13).

There is strong theoretical justification for extrapolation of the 'best-fit' line to the experimental lifetime data (Chapter 7). Also, a value for the creep strain rate of Kevlar 49 yarns and fibres, applicable to a wide range of low stresses, which is often quoted in the literature (6.5) is 0.02% per decade. This gives good agreement with the 'generated' creep rate curve which, at low stresses, levels-off at about 0.018% strain per decade. Hence, strong arguments in favour of extrapolating the experimental lifetime data exist but further tests to measure the creep strain rate of Parafil at low stresses ought to be undertaken.

6.5 Comparison with earlier work

6.5.1 Introduction

Little attention has been paid to determining the stress-rupture or creep characteristics of Kevlar ropes. The results of some limited work on ropes, however, have been reported by Wilkins (1975), Ferer (1977), and Bourgault (1980) while more recently Gerritse and Schurhoff (1986) have reported on the stress-rupture and creep behaviour of an aramid/epoxy 'strip'. The majority of work concerned with the stress-rupture of Kevlar has been confined to the acquisition of lifetime data for Kevlar 49 yarns; extensive use is made of this data in Chapter 7. A few authors (e.g. Ericksen 1984, Walton and Majumdar 1983) on the other hand, have concentrated solely on the creep strain behaviour of Kevlar 49.

6.5.2 Wilkins (1975)

Wilkins (1975) considered the long-term creep behaviour of several small Kevlar 49 tension members while assessing their suitability as the load bearing elements of electromechanical cables. Creep tests were carried out, at a stress equal to 50% of the initial ultimate strength, on 86 samples manufactured by U.S. Polymeric and Philadelphia Resins. The U.S. Polymeric members consisted of 30 yarns twisted into a helix and encapsulated in a urethane resin. The Philadelphia Resin members consisted of 7 composite strands arranged in a helix, each strand containing 3 straight yarns.

Wilkins (1975) observed many premature creep failures within the first 1000 hours of testing; these all occurred at a localised stress-point such as an end termination. For the surviving members, creep strain was found to be linear with the logarithm of time, similar to Parafil ropes, as shown in Figure 6.14. Creep strain rates of 0.026% and 0.039% per decade were observed for the U.S. Polymeric and Philadelphia Resins members respectively, whereas the corresponding value for Parafil (as predicted by the power law curve of Figure 6.12) is 0.005% per decade. The discrepancy accords with the large creep rates typically observed in ropes having a twisted construction (Horn et al.1977).

Using the assumption that both member types would fail in creep at the same relative elongations that were observed during short-term loading, Wilkins (1975) predicted the following lifetimes on the basis of the observed creep rates:

U.S. Polymeric	6.1×10^{35} years	at 50% of the
Philadelphia Resins	3.3×10^{16} years	initial ultimate load.

Wilkins (1975) admitted that the assumption of a constant failure strain, irrespective of the applied stress, was possibly wildly inaccurate. The incredible lifetimes predicted using this assumption, and the observations made on Parafil in which the strain at failure was found to decrease with reduced stress, strongly indicate that these results may be discounted.

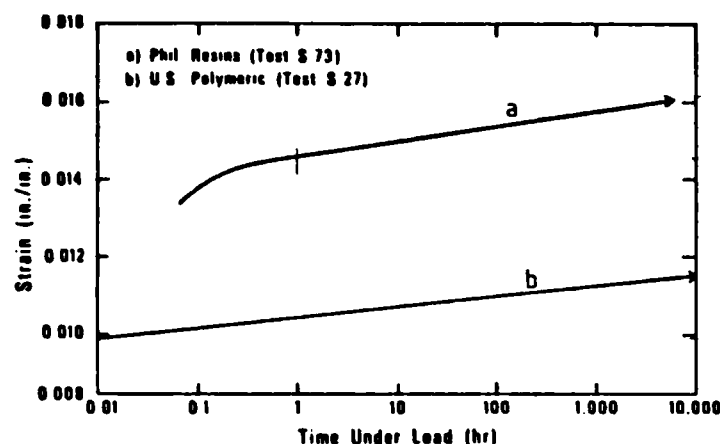


Figure 6.14 Logarithmic creep for two Kevlar 49 tension members (Wilkins 1975).

6.5.3 Ferer (1977) and Bourgault (1980)

Of greater relevance to Parafil is the work that has been conducted on a 0.25 inch diameter parallel-lay Kevlar rope called Uniline, produced by the Wall Rope Works, USA. Creep tests at stresses ranging from 25% to 95% of the ultimate stress have been performed on this material (and may be still in progress); the results have been reported by Ferer (1977) and Bourgault (1980).

Figure 6.15 shows the set of creep curves obtained for Uniline; the intrinsic shape of the curves is remarkably similar to that observed when testing Parafil ropes (Figure 6.7). A linear dependence of the creep strain with the logarithm of time is first displayed followed by an upward sweep as failure is approached. Also, as the applied stress was reduced, the creep strain at failure was found to decrease although (similar to Parafil) the creep strain capacity was found to increase.

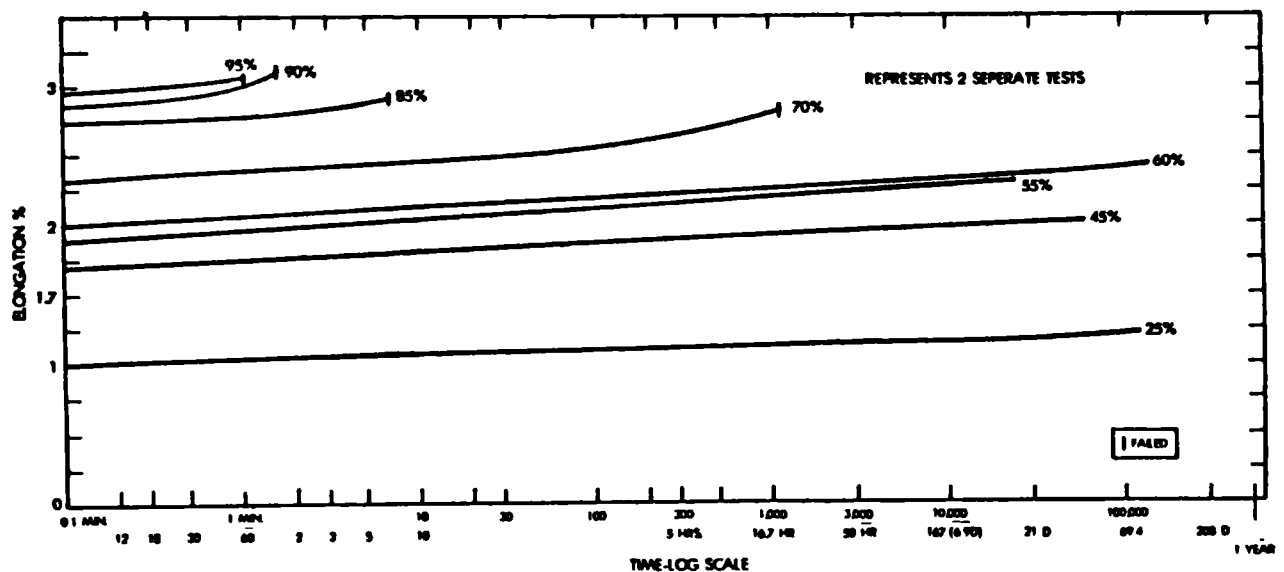


Figure 6.15 Creep and stress-rupture behaviour of Uniline (Ferer 1977).

Bourgault (1980) indicates (Figure 6.16) that the rate of creep strain (for Uniline) may have increased even more rapidly just prior to failure, than indicated by the smooth curves of Ferer (1977). A sudden increase in the creep rate at the end of the secondary creep phase is also a characteristic of Parafil Type G ropes.

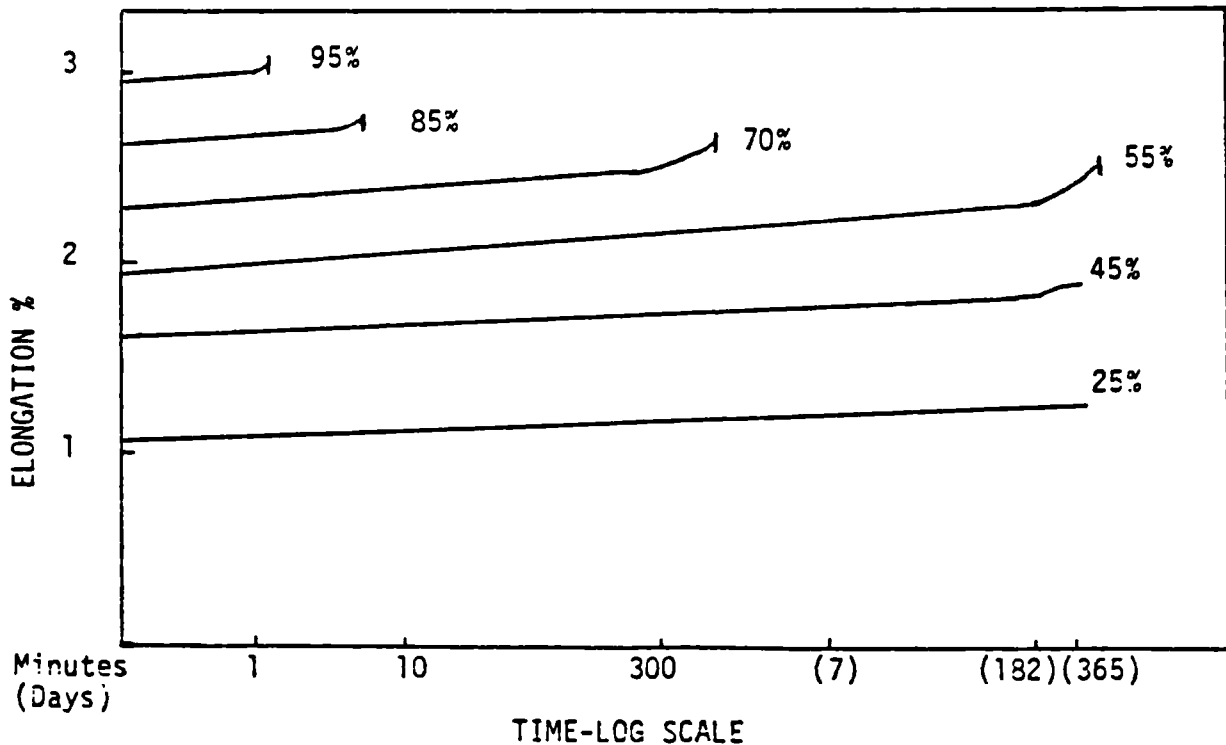


Figure 6.16 Creep and stress-rupture behaviour of Uniline (Bourgault 1980).

The dependency of the 'time to break' on applied stress for the Uniline rope is illustrated in Figure 6.17 together with the results for Parafil. If the very short lifetime observed at 95% of the initial ultimate stress is discounted from the Uniline data then a linear dependency of lifetime on applied stress can also be interpreted. Parafil, however, outperforms Uniline; when the results

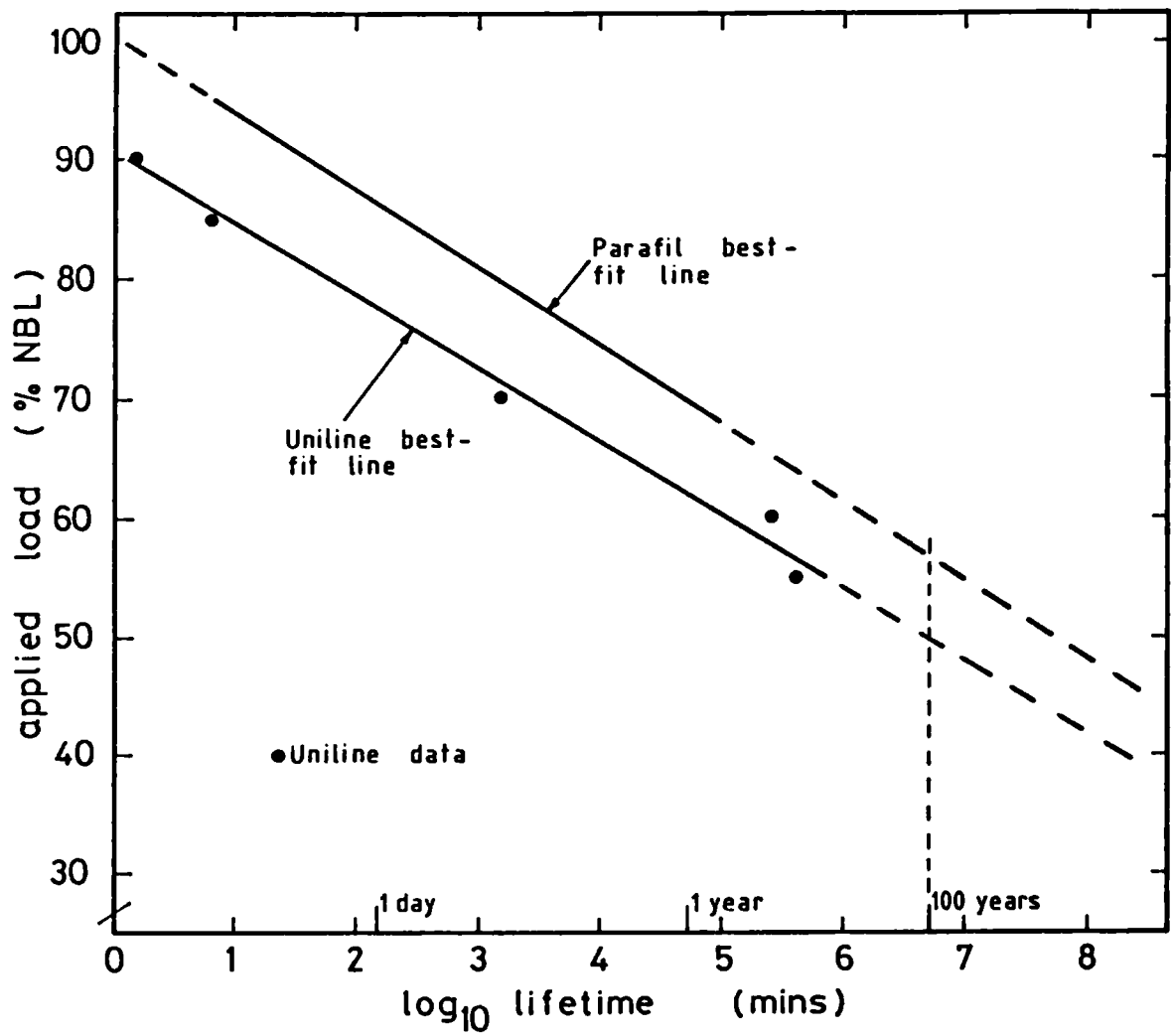


Figure 6.17 Comparison between lifetime data for Uniline and Parafil.

are extrapolated, a stress equivalent to 44% of Uniline's initial ultimate strength produces a creep failure after a typical structural design life of 100 years whereas the corresponding figure for Parafil is 50%. Although Ferer (1977) and Bourgault (1980) use the generic term 'Kevlar' in their descriptions of the Uniline work it is probable that Uniline is based on Kevlar 29 since the high initial strains recorded in the creep tests (Figure 6.16) are more compatible with the lower modulus material. The adoption of different core yarns, therefore, provides an obvious reason for the difference in stress-rupture lifetimes between Parafil and Uniline although the type of termination employed is likely to be a further factor.

6.5.4 Gerritse and Schurhoff (1986)

Gerritse and Schurhoff (1986) have considered the structural applications of a solid aramid/epoxy composite in continuous strip form. The aramid, called Twaron, has properties remarkably similar to Du Pont's Kevlar and is similarly available in both low and high modulus form.

A creep rate of 0.025% per decade at 50% of the ultimate tensile strength is quoted for the higher modulus material which agrees reasonably well with the corresponding value for Parafil extracted from the 'generated' creep rate curve shown in Figure 6.12. The behaviour of the composite in stress-rupture appears to resemble closely that of Parafil Type G tendons. However, since Gerritse and Schurhoff (1986) have adopted a solid line (projected to 100 years) in their stress-rupture plot to portray the behaviour of Twaron it is impossible to determine the extent (if any) to which actual experiments have been performed.

Gerritse and Schurhoff (1986) propose to use this material as a tendon in prestressed concrete members bonded directly to the concrete. Owing to the problems of creep in epoxy resins, the susceptibility of aramids to chemical degradation when in intimate contact with highly alkaline substances, and the brittle mode of failure displayed by aramids, it is likely that practical difficulties will arise when the material is used in this manner.

6.5.5 Ericksen (1984)

Considerable lifetime data is available for Kevlar 49 yarns (Chapter 7) yet surprisingly little information is available concerning the creep of Kevlar 49 yarns or fibres. A creep rate of 0.02% per decade, independent of stress, is frequently quoted (e.g. Horn et al. 1977, Du Pont 1980, Howard and Parratt 1985) and this seems to summarise the extent of the majority of the research on creep.

Ericksen (1984), however, has investigated the creep of Kevlar 49 fibres in greater detail including a study of creep recovery, modulus changes and possible creep mechanisms. Ericksen (1984) found that the creep of Kevlar could be modelled by the equation:

$$\epsilon(t) = \epsilon_1(\sigma, \tau) + \alpha(\sigma, \tau) \log_{10} t \quad 6.7$$

where: ϵ_1 is the strain after unit time

$\alpha(\sigma, \tau)$ is a rate parameter which is independent of time but varies with load, temperature and environmental variables. α is the slope of the strain versus log. time plot for constant load creep.

Equation 6.7 is consistent with the empirical approach proposed for making lifetime predictions for Parafil based on a strain criterion (Equation 6.6). Indeed, the parameter α of Ericksen's equation is directly comparable with the parameter, r , which was used to define the creep rate for Parafil as a function of stress.

In defining a rate parameter α , Ericksen (1984) clearly disagrees with the idea of a constant rate of creep independent of the stress. Figure 6.18 indicates how the creep rate (α) for Kevlar 49 filaments varies with the applied stress; on a log-log plot an increasing trend is apparent despite the considerable scatter. Ericksen (1984) did not resolve the details of the stress dependence, probably owing to the extent of the scatter. However, log-log coordinates are often used for plotting data when there is a power law relationship between the two variables. There exists some independent experimental evidence, therefore, that a power law of the form used to derive Equation 6.5, is appropriate for describing the dependence of the creep rate on stress across the complete stress range. Also shown in Figure 6.18

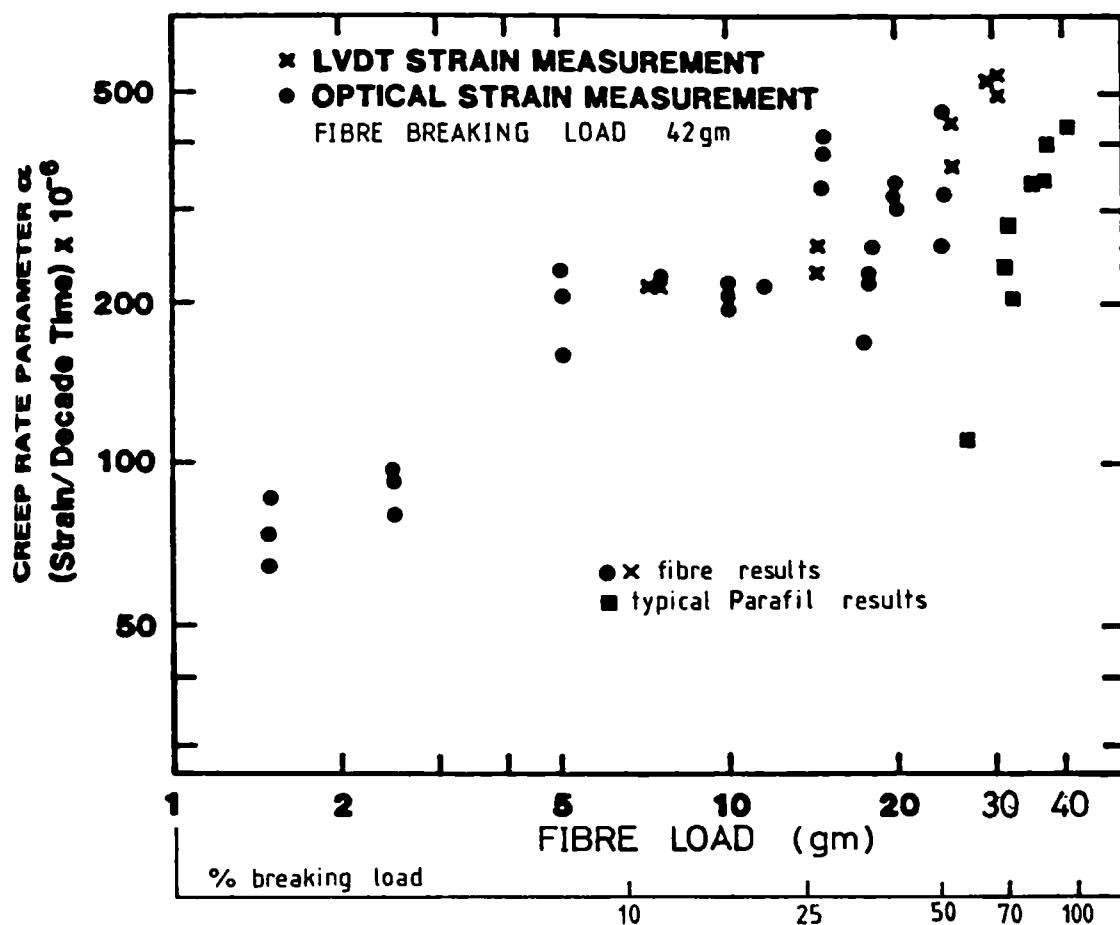


Figure 6.18 Log-log plots of creep rate parameter versus load for Kevlar 49 fibres (Ericksen 1984) and Parafil Type G ropes.

are the results for Parafil when normalised with respect to initial breaking strength; there is poor agreement with the results for filaments. Nevertheless, the strong linear trend between log. creep rate and the log. of the applied load indicates that a power law description (Figure 6.12) for the creep rate of Parafil is justified; the linearity formed the basis for the determination of the two power law constants of Equation 6.5.

6.5.6 Walton and Majumdar (1983)

The creep strain response of Kevlar 49 filaments has also been assessed by Walton and Majumdar (1983). The results of the study are shown in Figure 6.19. Despite the virtually linear creep response with log. time Walton and Majumdar (1983) preferred to model the behaviour using a power law of the form At^n where A is a function of stress and n is a constant.

However, if the creep response were assumed to be linear over the five complete decades of time shown in Figure 6.19 then Table 6.2 may be used to find an approximate creep rate for Kevlar 49 filaments at various levels of stress. Also shown in Table 6.2 are the creep rates for Parafil Type G ropes predicted using Equation 6.5; at low stresses the creep rate predicted for Parafil is much less than that for the fibres.

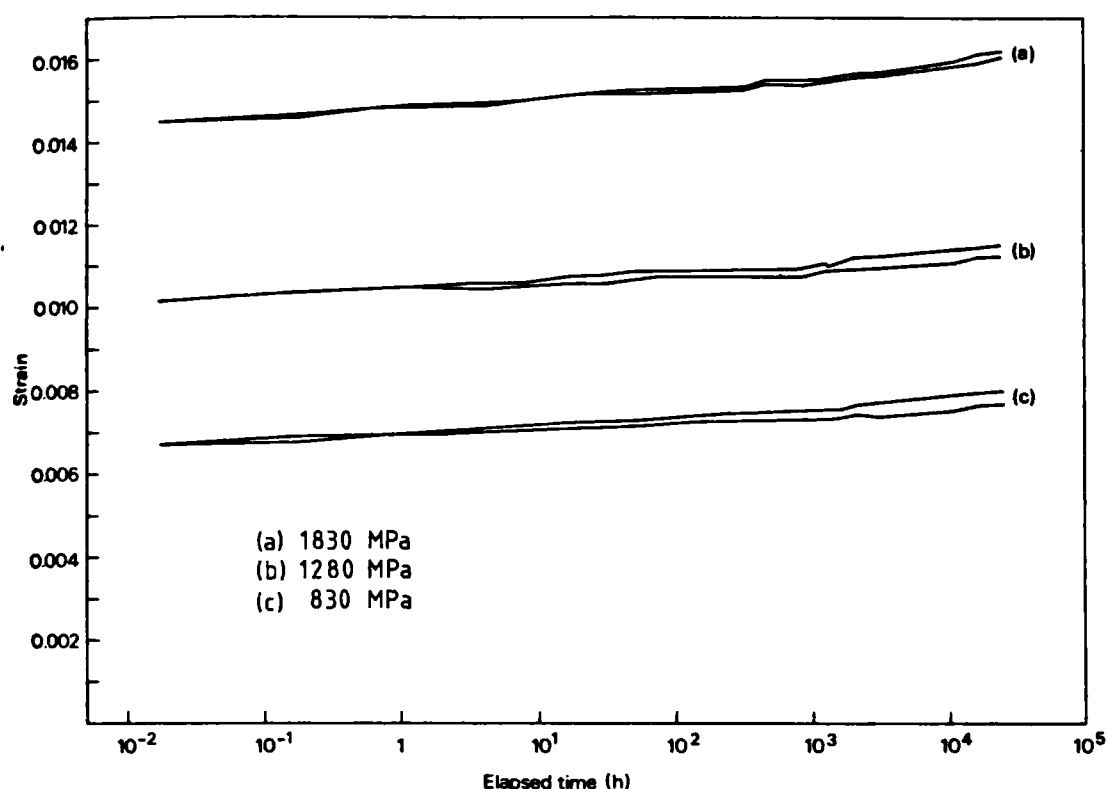


Figure 6.19 Logarithmic creep in Kevlar 49 fibres
(Walton and Majumdar 1983).

Stress (MPa)	Approx % of initial strength	Creep rate for Kevlar 49 filaments (% strain/decade)	Predicted creep rate for Parafil (% strain/decade)
830	29	0.015	0.001
1280	44	0.017	0.003
1830	63	0.022	0.012
2425	84	failed in < 3 minutes	0.032

Table 6.2 Approximate creep rate data for Kevlar 49 filaments based on Walton and Majumdar (1983) and corresponding predicted rates for Parafil Type G ropes.

The results of Walton and Majumdar (1983) for filaments are significantly different to those predicted for Parafil ropes (using Equation 6.5) since they indicate only a small dependence of creep rate on the applied stress. Indeed, the well-quoted figure of 0.02% creep strain per decade, independent of stress, appears acceptable on the basis of their tests. However, there is good agreement between the results of Walton and Majumdar (1983) and the Parafil creep rate data generated from experimental lifetime results (Figure 6.12). The agreement provides further justification for the extrapolation of the 'best-fit' line to the lifetime data (Figure 6.13) and casts doubt on the assumed power law relationship for Parafil creep rates.

6.6 Appraisal of the empirical approaches for predicting long term behaviour under static loading

The method of predicting long-term stress-rupture lifetimes, based on strain measurements taken during a series of relatively short-term tests, should be used guardedly. For instance, the notion of a creep-strain capacity which increases only marginally as the applied stress is reduced, is purely empirical and cannot be supported theoretically. Also, lifetime predictions for Parafil at typical operating stresses (using this method) are very dependent on the assumed creep rate. In general, the creep rates quoted for Kevlar 49 fibres at the lower end of the stress scale, exceed those for Parafil determined from the power law (Equation 6.5). Also, the long-term stress-relaxation rates (Chapter 5) were found to be broadly similar in tests conducted between 30% and 70% of the nominal breaking stress. There are, therefore, doubts as to the validity of the assumed power law to describe creep rates for Parafil outside the investigated stress range and so for the time being stress-rupture predictions for Parafil based on strain measurements should be considered as optimistic.

A linear extrapolation of the 'best-fit' line for the lifetime data, on the other hand, seems well justified. A straight line not only fits the data reasonably well but is consistent with reaction rate theories for creep; these theories are fully described in Chapter 7. Furthermore, a linear dependence of the logarithm of the lifetime on stress has been confirmed in tests lasting up to 18 months on a Kevlar rope of similar construction (6.5.3). Further tests on Parafil, lasting years rather than weeks are clearly desirable; the present test rig based on hydraulically applied loads would be unsuitable for these tests. However, even if the linear trend were found to continue at the lower stress levels, the problem of determining any degree of reliability or probability of failure would remain unless a very large number of tests were undertaken.

CHAPTER 7: MODELS FOR DESCRIBING STRESS-RUPTURE AND THE DETERMINATION OF OPERATING STRESSES

7.1 Introduction

The problem of reliably predicting the long-term behaviour of Parafil ropes when subjected to dead-loading could be resolved by conducting a large number of tests. Probability curves could then be constructed from the test data to allow designers to establish an operating stress that would result in an acceptable probability of survival at the end of the chosen structural design life. Such a testing programme, however, would be very expensive in terms of money and time, with some tests taking many years to complete.

It follows that predictions for the long-term behaviour of Parafil, based solely on the limited number of stress-rupture tests conducted so far (Chapter 6) would be largely unacceptable. However, if the results could be shown to conform to a recognised theory of stress-rupture or to agree with the experimental models of other investigators then any long-term predictions could be regarded with more confidence.

Increasing attention has recently been focussed on the study of the time-dependent failure of polymers owing to the growing use of polymers in engineering applications. The subject has, however, been of concern to researchers for the past forty years and from the literature generated over this period it is apparent that the failure of polymers under sustained loading can be described by models based broadly on:

- (1) absolute reaction rates
- (2) modified reaction rates
- (3) the existence of a critical stress or threshold level (below which time-dependent failure does not occur)
- (4) crack-nucleation
- (5) mathematical descriptions of experimental results.

In this chapter a review of each of these models is made since they all contain arguments upon which predictions for the long-term

behaviour of Parafil Type G ropes could be based. Applications of each model are considered wherever possible, especially in relation to Kevlar 49. A statistical model based on stress-rupture data for impregnated yarns of Kevlar 49 is favoured, however, for predicting the behaviour of Parafil. Extensive use is made of this statistical model which not only conforms reasonably well with the stress-rupture data for Parafil (Chapter 6) but enables a rational, probabilistic approach to structural design to be adopted.

The concept of cumulative damage for predicting the stress-rupture behaviour of polymers when subject to time-varying loads is discussed. This concept is incorporated into a computer program, along with the statistical model and various descriptions for the loading history of a Parafil Type G tendon, to enable operating stresses for Parafil to be maximised.

The effects of temperature on the integrity of structural members incorporating Parafil tendons is considered. Finally, the effects of various environmental conditions on the stress-rupture behaviour of Kevlar 49 and their implications for Parafil are discussed.

7.2 The absolute reaction rate approach

The development of the theory of reaction rates is founded largely on a proposal by Arrhenius in 1889 to account for the influence of temperature on the rate of inversion of sucrose. The proposal, now known simply as the Arrhenius Equation, was of the form:

$$K = A e^{-E/RT} \quad 7.1$$

where: K is the specific rate of the reaction

A is the frequency factor (constant)

E is the energy of activation of the reaction (constant)

R is the gas constant

T is the temperature.

Glasstone et al.(1941) postulated that the theory of absolute reaction rates was not merely a theory of the kinetics of chemical reactions but in principle could be applied to any rate process involving a

rearrangement of matter, whether it be physical or chemical. Indeed, Glasstone et al.(1941) proceeded to use the method to describe the phenomena of viscosity and diffusion in fluids, and also electromechanical processes.

It was Tobolsky and Eyring (1943) however, who were the first to apply a reaction rate model to the fracture of polymers. It was assumed that the failure of polymeric threads under load was attributable to the slipping of bonds. If the number of bonds per unit area were N then the rate of failure of bonds under a stress σ (assuming that bond repair was also possible) was judged to be given by:

$$-\frac{1}{N} \frac{dN}{dt} = \frac{k}{h} T \exp(-E/RT) 2 \sinh \left(\frac{\sigma \lambda}{2NkT} \right) \quad 7.2$$

where: k is Boltzmann's constant

h is Planck's constant

λ is the mean separation between the equilibrium positions of minimum force potential

σ is the stress

E, R, T are as defined in Equation 7.1.

The lifetimes of polymeric threads under this model are obtained by integrating Equation 7.2 between the limits of zero and N_0 (the initial number of bonds per cross-section).

The model of Tobolsky and Eyring (1943) was successfully used by Graham et al.(1969) to describe the rupture of bonds at the interface of an inert filler and a polymer matrix. It was shown that the determination of three constants was sufficient to model constant load creep-failure. The good agreement between the computed failure times (based on 'best' estimates of the three constants) and the experimental failure times is shown in Figure 7.1. A linear relationship between applied stress and the logarithm of lifetime is both predicted and observed. Graham et al.(1969) proceeded to demonstrate that the same three material constants could be used to adequately predict material behaviour under varying load histories.

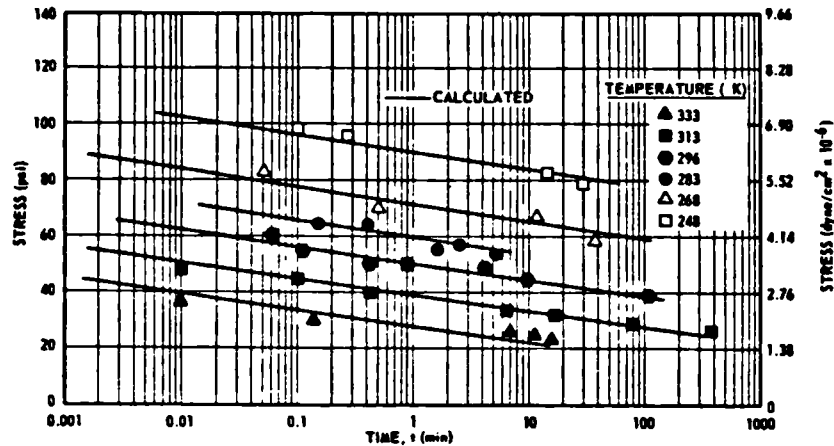


Figure 7.1 Stress-rupture behaviour for an interface of an inert filler and polymer matrix (Graham et al.1969).

Coleman and Knox (1957) also used an absolute reaction rate approach to describe the time-dependent nature of creep failure in textile fibres. The approach differed from that of Tobolsky and Eyring (1943) in that the rate-determining step in the mechanical breakdown of a fibre under load was considered to be the displacement of a set of entities called force centres rather than the rupture of bonds. The mean relative displacement of the force centres at time t is measured by a quantity called γ which is initially zero. The time required for γ to reach a critical value γ_B , at which point the process of breakdown becomes rapid, is regarded as the lifetime (t_B) of the material.

The rate of increase of γ is governed by the differential equation:

$$\frac{d\gamma}{dt} = \frac{2\lambda kT}{h} \exp(-E/RT) \sinh \left\{ \frac{\sigma(t)\delta}{2kT} \right\} \quad 7.3$$

where δ is equal to the product of the effective cross-sectional area per force centre and the jump distance
(see Equations 7.1, 7.2 for definition of remaining parameters).

There is obvious similarity between Equations 7.2 and 7.3. Indeed, for constant load experiments at high stress levels, the two models yield identical results (Henderson et al.1970).

If equation 7.3 is integrated subject to the boundary conditions:

$$\text{at } t = 0 \quad \gamma = 0$$

$$\text{at } t = t_B \quad \gamma = \gamma_B$$

and if it is assumed that $\sigma \gg 2 kT/\delta$, then for dead-load conditions, it can be shown that:

$$\ln t_B = \ln A - B\sigma \quad 7.4$$

where A and B are constants.

Hence, a linear relationship between the applied stress and the logarithm of the 'time to break' is also a characteristic of this model; the linearity provides further justification for the extrapolation of the 'best-fit' line to the experimental stress-rupture data for Parafil (6.4). Coleman and Knox (1957) suggest that if the constants A and B are determined from a series of dead-loading experiments, similar to those conducted on Parafil, then lifetimes under periodic loads, linearly increasing loads, or indeed any loading history, can be predicted using Equation 7.3. Figure 7.2 shows a series of experimental plots of log.time versus stress for various types of nylon 66 fibres from which Coleman (1956) was able to determine the constants A and B (for this particular material) easily.

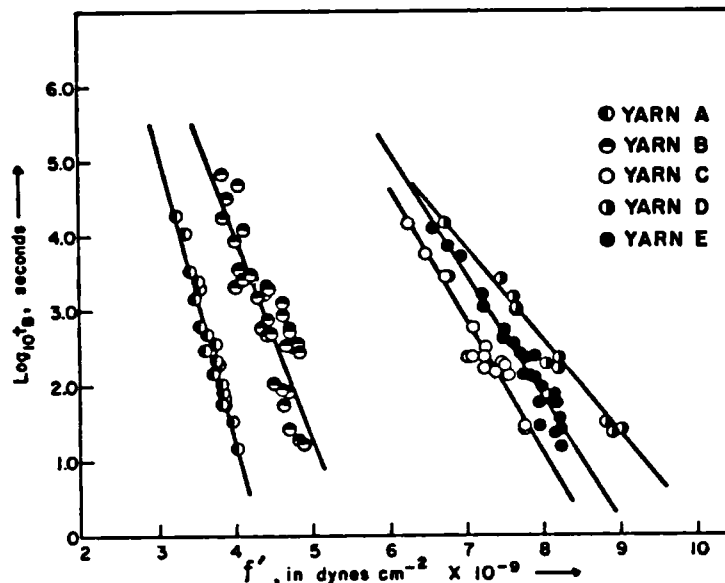


Figure 7.2 Linear dependence of lifetime on stress for drawn nylon 66 yarns of varying draw ratio (Coleman 1956).

Unless experiments are conducted, Equation 7.3 cannot be readily used to predict the stress-rupture behaviour of a particular polymer since it includes parameters which cannot be simply evaluated. For this reason, Coleman and Knox (1957) pursued an empirical approach (Equation 7.4). Wilfong and Zimmerman (1977), however, propose a method by which the fundamental differential equation (Equation 7.3) can be used directly.

The method of Wilfong and Zimmerman (1977), which is based largely on an assumption about the number of force-centre jumps required to produce failure, enables a relationship between log. lifetime and the applied stress to be calculated. The details of the method, when applied to Kevlar, are given in Appendix 3.

It will later be shown (7.7.4) that valid comparisons between stress-rupture results for Kevlar 49 (or Kevlar 49 composites) and Parafil Type G ropes can be made if the results are normalised with respect to ultimate tensile strength. Nevertheless, Figure 7.3 indicates, in this instance, poor agreement between the theoretical results for Kevlar and the experimental results for Parafil. For example, the direct use of Equation 7.3 (line b) indicates that a stress equal to 65% of Parafil's breaking strength would cause creep failure after 100 years whereas the corresponding value given by the 'best-fit' line for the experimental results (line a) is 50%.

7.3 The modified reaction rate approach

The model developed by Coleman and Knox (1957) was admittedly approximate and its simplicity was intended to be provocative. In particular, Lyons (1958) disagreed with the suitability of the Coleman model for describing the behaviour of polymers when subjected to cyclic loads and a minor modification to the theory was suggested. Later, Prevorsek and Lyons (1964) disagreed with the whole concept of molecular flow and proposed a fracture mechanism based on a crack-nucleation process (7.5).

However, a major modification to the absolute reaction rate approach was proposed by Zhurkov (1965) and further developed by Zhurkov and Korsukov (1974). Zhurkov (1965) proposed a modified Arrhenius equation

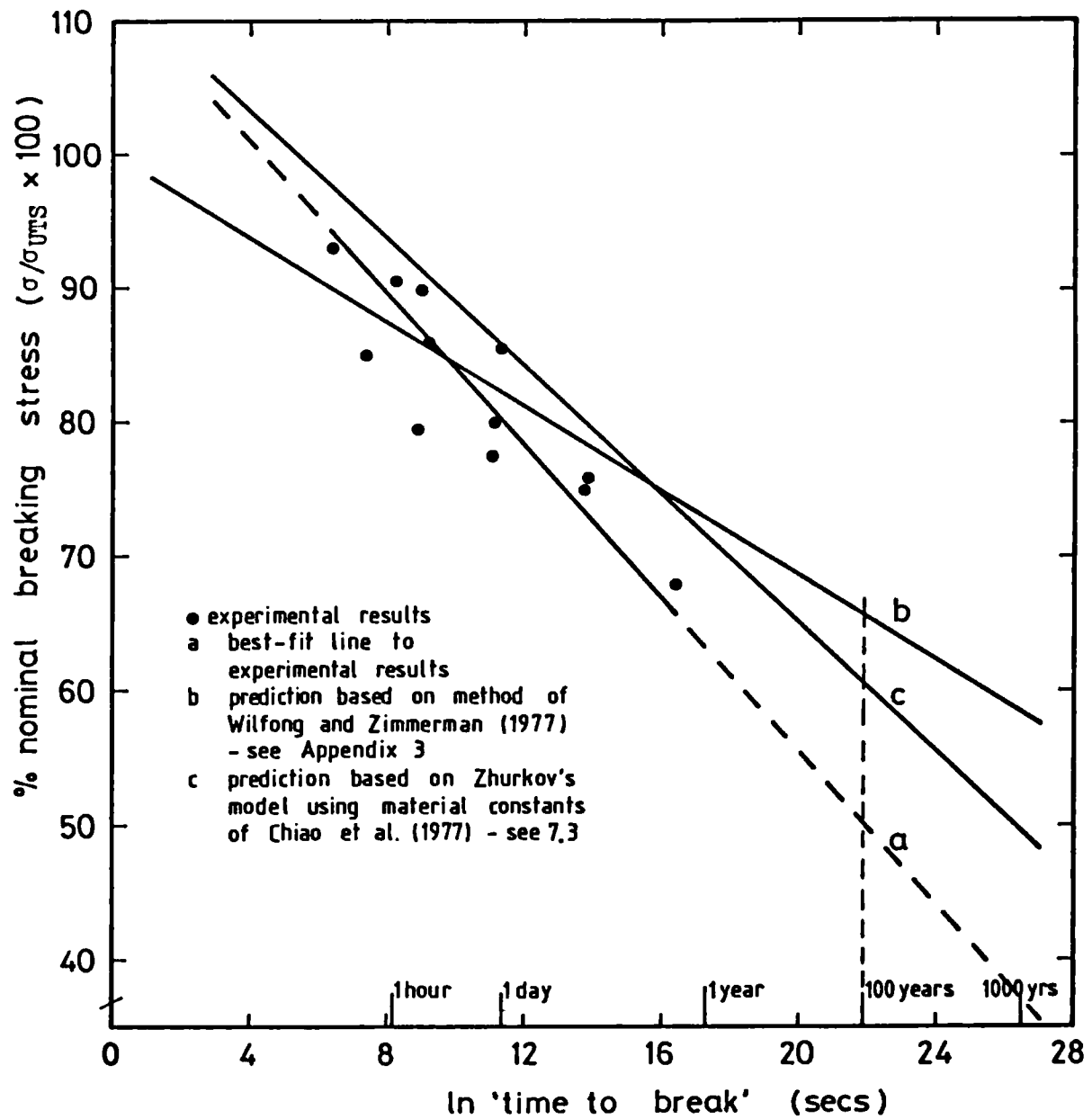


Figure 7.3 A comparison between the experimental stress-rupture results for Parafil and two theoretical predictions.

of the form:

$$K = A \exp \left\{ - (E - \alpha\sigma)/RT \right\} \quad 7.5$$

where α is a constant.

The modification allowed for a dependence of the activation energy, E , on the applied tensile stress, σ . Zhurkov (1965) was able to show that the modified approach adequately predicted the stress-rupture behaviour of some fifty materials and hence, that the activation energy required to cause failure decreased with increased stress.

Zhurkov and Korsukov (1974) also addressed the problem of whether molecular chain scission or molecular slippage was the primary cause of polymer failure. The results of tests based on electroparamagnetic resonance and infrared spectroscopy favoured, in their opinion, molecular chain scission.

An additional modified reaction rate model for describing the stress-rupture behaviour of polymers has been developed by Kausch et al.(1967) and Hsiao et al.(1968). The model is similar to that of Zhurkov (1965) in that it favours bond rupture (rather than molecular slippage), and also allows for the effect of mechanical stress on the activation energy. An additional term was included in the governing differential equation, however, to allow for the possibility of bond reformation. The additional term was thought necessary to account for the extremely long lifetimes of solids at low stress levels. The model involves the determination of six constants and so is perhaps not as easily validated as those described earlier.

Chiao et al.(1977), however, were able to verify Zhurkov's model experimentally by conducting accelerated stress-rupture tests (at high temperatures and stresses) on Kevlar 49/epoxy resin composite yarns.

Zhurkov's modified approach may be represented as:

$$t_B = t_0 \exp [(E - \alpha\sigma)/RT] \quad 7.6$$

where: t_B is the time to break
 t_0 is the period of the thermal oscillations of bonded atoms
 α is a coefficient related to the stress on atomic bonds
 E, R, T are as defined in Equation 7.1.

Chiao et al.(1976) obtained the following values for the material constants for Kevlar 49/epoxy composites:

$$\begin{aligned} t_0 &= 2.844 \times 10^{-7} \text{ secs} \\ E &= 151908 \text{ J/mole} \\ \alpha &= 1022 \text{ J/mole.} \end{aligned}$$

If these values are substituted into Equation 7.6 and logarithms are taken, then:

$$\ln t_B = 47.29 - 41.95 \frac{\sigma}{\sigma_{UTS}} \quad 7.7$$

where: σ_{UTS} is the ultimate tensile strength.

Despite the modification to the reaction rate theory a linear relationship between the applied stress and the logarithm of lifetime is still predicted. The results from this model are plotted in Figure 7.3 (line c). For time periods in excess of 76 days the lifetimes predicted (using the parameters of Chiao et al.1976) are intermediate to those given by the method of Wilfong and Zimmerman (1977) and the experimental 'best-fit' line for Parafil.

Cook et al.(1982) and Howard and Parratt (1985), after conducting stress-rupture tests on bare Kevlar 49 yarns, found that there was a linear relationship between the logarithm of lifetime and the square of the applied stress. The mechanism of breakdown was still considered to be thermally activated but the energy required for the process was assumed to reduce with the square of the applied stress as opposed to the linear reduction proposed by Zhurkov (1965). The use of a stress-squared term supposedly emphasised the role of stored elastic energy, rather than bond strain, in promoting fracture. However, when the experimental results for Parafil are plotted in this manner, a linear relationship between stress-squared and log. lifetime does not appear convincing.

Phoenix and Wu (1983) have analysed the results of stress-rupture tests conducted on Kevlar 49/epoxy yarns over a ten year period at the Lawrence Livermore National Laboratory. It was concluded that the appropriate way to plot the stress-rupture data was on a log. lifetime /log. stress basis. This seems to imply that the fracture of Kevlar under static loading is dependent on a power law breakdown rule rather than an exponential one. For this particular material, however, it so happens that the two breakdown rules yield similar results (Phoenix 1978). In fact, an alternative but extremely thorough statistical analysis of the same Lawrence Livermore data conducted by Glaser et al.(1984) favours the adoption of stress versus log. lifetime coordinates (7.6).

7.4 The critical stress concept

Wilding and Ward (1978, 1981, 1981a) have pursued the notion of an apparent critical stress in highly oriented polymers below which the phenomenon of stress-rupture does not exist. These investigators conducted creep tests on ultra high modulus polyethylenes of various molecular weights. In the case of polyethylenes of high molecular weight it was found that a stress existed below which the long term creep-rate fell to a negligible amount; this was termed the critical stress. Furthermore, when the creep load was removed from such samples full creep recovery was observed (within the resolution of the testing apparatus).

It is suggested by these researchers that the existence of a critical stress can be determined by plotting the creep strain-rate against the creep strain as shown for high modulus polyethylenes in Figure 7.4. In the case of the polyethylene of low molecular weight, the creep-rate eventually reaches a steady-state value signifying the presence of a flow process. The flow was thought to be adequately described by a thermally activated rate process such as that proposed by Coleman and Knox (1957). In contrast, the high molecular weight material does not display a plateau strain-rate except at high stresses. At the lower stresses, the flow process of plastic deformation is not activated, indicating that the applied stresses are less than the critical value.

that if the creep tests conducted on the high molecular weight polyethylenes at low stress levels (Wilding and Ward 1981) had been allowed to continue for a sufficient length of time, then these too would have eventually exhibited a plateau strain-rate, thus precluding the existence of a critical stress.

7.5 Crack-nucleation

It became apparent to Prevorsek and Lyons (1964) that a molecular mechanism for the rupture of fibrous polymers, based on the viscous flow of molecules, provided an inadequate description under certain conditions of fatiguing. Instead, a brittle fracture, initiated from a crack, was postulated as a mechanism for the rupture of polymers subject to fatigue loading. Failure was ascribed to the slow growth of cracks which, upon reaching a critical size, became unstable and propagated across the specimen in accordance with Griffith's hypothesis (Griffith 1921).

Despite this seemingly independent approach, the details of the actual theory still revolved around the theory of reaction rates. In this case, however, rate theory was used to model the rate of growth of cracks as opposed to the rate of failure of bonds (Tobolsky and Eyring 1943) or the rate of displacement of force centres (Coleman and Knox 1957). The time to failure was thus approximated to the time required for the formation of cracks up to the critical size.

Prevorsek (1966) extended the previous work on crack-nucleation to cover more general forms of tensile loading while still regarding the slow growth of cracks as a thermally activated rate process. An expression for the time to failure of a polymer under a constant stress was obtained and then a law of cumulative damage (7.8) was assumed in order to investigate material behaviour under time-varying loads. Prevorsek (1966) found experimental verification of the crack-nucleation theory difficult, however, primarily because it involves parameters that cannot easily be determined such as fracture surface work and stress-concentration factors. For this reason, there is little likelihood that this method will be of practical benefit in predicting the stress-rupture behaviour of Parafil Type G ropes.

More recently, Christensen (1981) and Christensen and Glaser (1983) used kinetic fracture mechanics (flaw growth theory) to model the accumulation of damage in Kevlar 49/epoxy composites tested at the Lawrence Livermore National Laboratory. The model was successfully used to predict the strength retention of the composites after a period of sustained dead-loading and also to relate the statistical parameters of lifetime to those for short-term tensile strength. In agreement with Prevorsek (1966) these authors found that stress-rupture lifetime was dependent on the time taken for a slow-growing crack to reach a critical size, whereupon an appreciable change in crack size occurred leading to sudden failure.

7.6 Mathematical descriptions of experimental results

An alternative to models based on actual material mechanisms is the mathematical model of experimental data. Mathematical models, unlike the models discussed earlier are relevant to a particular material only and must, by necessity, be based on a large number of tests. A mathematical model to describe the stress-rupture behaviour of Kevlar 49/epoxy yarns has been formulated by Glaser et al.(1984). Similarly, Howard and Parratt (1985) have performed a basic statistical appraisal of a large number of stress-rupture results for bare Kevlar 49 yarns at elevated temperature.

Glaser et al.(1984) have summarised the data for the Kevlar 49/epoxy yarn stress-rupture tests conducted at the Lawrence Livermore National Laboratory (LLNL) between 1971 and 1986. Three classes of experiment were performed:

- (1) dead-load tests on 600 yarns under ambient conditions but subject to regular fluorescent lighting
- (2) dead-load tests on 900 specimens, in the dark, but at elevated temperatures
- (3) pressure vessel tests in the dark, under ambient conditions, involving 250 vessels.

The lifetime data was analysed statistically (see Appendix 4) assuming that it followed a two-parameter Weibull distribution with stress-dependent parameters α and β . (Incidentally, this assumption is supported by the flaw-growth theory of Christensen and Glaser 1983).

The stress-dependency of transformed Weibull parameters, a and b , was then modelled polynomially using the method of maximum likelihoods (Cohen 1965).

The great advantage of this statistical approach is that contour lines of failure probability may be drawn which permit the selection of a constant stress level that would provide an adequate probability of survival after a given time. This is precisely the sort of information that engineers require for rational design. Figure 7.5 shows the family of curves for the pressure vessel tests which will form the basis for the determination of the long-term stress-rupture behaviour of Parafil Type G ropes (7.7).

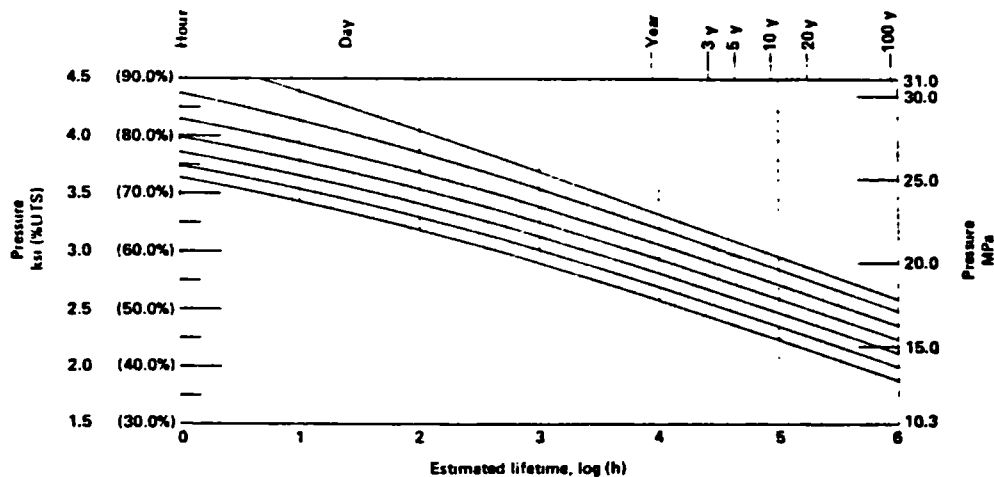


Figure 7.5 Family of probability of failure curves for pressure vessels of impregnated Kevlar 49 yarns based on worst fibre spool - probabilities from left to right are 10^{-6} , 10^{-5} , 10^{-4} , 10^{-3} , 10^{-2} , 10^{-1} , 0.50 (Glaser et al.1984).

The results from considerable work on the stress-rupture behaviour of bare Kevlar 49 yarns (as opposed to composite yarns) have been reported by Cook et al.(1982), Howard (1983), and Howard and Parratt (1985). The tests were conducted using a split-ring device (Howard 1983) with the imposed load being dependent on the initial closing force imparted to the ring. A statistical treatment of 370 stress-

rupture results from tests conducted at elevated temperatures (65°C, 110°C and 150°C), coupled with a modified reaction rate approach, enabled the stress-rupture behaviour of Kevlar 49 yarns, at temperatures between 23°C and 200°C and at stress levels between 10% and 70% of the ultimate strength, to be predicted. Figure 7.6 shows the resulting stress-rupture master curve. The results of this work provide further experimental evidence in favour of the adoption of the LLNL statistical model for predicting Parafil stress-rupture behaviour as discussed in 7.7.3.

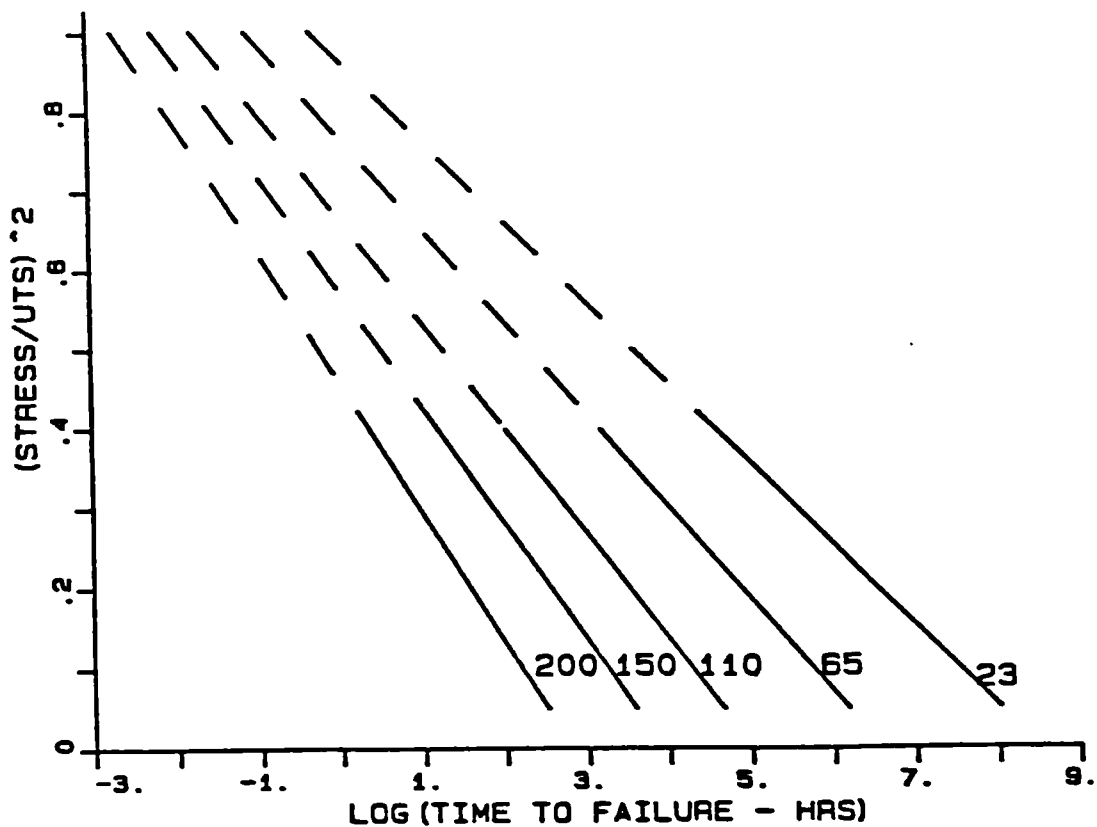


Figure 7.6 Master stress-rupture curve for bare Kevlar 49 yarns determined from tests at elevated temperatures (Howard and Parratt 1985).

7.7 The suitability of the LLNL data for predicting the long-term stress-rupture behaviour of Parafil Type G ropes.

7.7.1 Introduction

The main purpose of any stress-rupture theory is to make predictions for lifetime beyond the range of available data. For Parafil, an extrapolation of the experimental 'best-fit' line could be justified on the basis of the various reaction rate models which predict a linear relationship between applied stress and the logarithm of time. These models, however, can only lead to an indication of 'average' behaviour which would form a rather poor basis for the rational design of concrete structures incorporating Parafil tendons.

Ideally, structural design should be probabilistic rather than deterministic; i.e. the design should be based on an acceptable probability of failure rather than a widespread application of safety factors to 'average' behaviour. Besides, the determination of suitable safety factors is itself problematical and highly subjective, and cannot be sensibly achieved without some knowledge of the scatter of experimental results.

The mathematical model proposed by Glaser et al.(1984) is a serious attempt to provide engineer's with exactly the right sort of data needed to ensure the rational design of structures incorporating Kevlar 49. Unfortunately, the model is based on Kevlar 49/epoxy yarns and so obviously the data cannot be applied to Parafil ropes directly. However, the data is adaptable if several well-judged assumptions are made. The limitations on using the results directly include:

- (1) relating the results from pressure vessel tests to conventional tests on straight yarns
- (2) relating the results for impregnated yarns to bare yarns
- (3) relating the results for yarns to ropes.

7.7.2 Applicability of the pressure vessel tests

Design forecasts for Parafil, based on yarn specimens subjected to elevated temperature or the degrading effects of ultra-violet radiation would be inappropriate and probably erroneous, for the

application under consideration here. Of the three series of tests conducted at LLNL, the pressure vessel tests conducted in the dark under ambient conditions, are therefore, the most pertinent to the application of Parafil in prestressed concrete. The pressure vessel test results are not, however, without complication. For instance, in pressure vessels, significant stresses are generated perpendicular to the yarn direction which would not be present in a conventional uniaxial rope application. Phoenix and Wu (1983) have shown, however, that there is little difference in the mode of behaviour between the LLNL pressure vessel specimens and straight yarns, despite the presence of multiaxial stress fields.

The pressure vessel test results are further complicated since they tend to give rise to slightly more optimistic predictions than normal yarn tests. Glaser et al.(1984) suggest that the slight fluctuations in ultimate strength between different spools of yarn may account for this, but Phoenix and Wu (1983) are of the opinion that the slow rate of loading employed in the ultimate strength tests for pressure vessels, leads to a reduced 'short-term' strength such that the level of stress (as a proportion of the short-term strength) is over-estimated. Gourdin (1980), on the other hand, found that the loading rate had little influence on the strength of Kevlar 49 (although the range of loading-rates investigated was rather limited).

In the LLNL test programme, eight different spools of fibre were used for pressure vessel fabrication. Hence, to account for the slightly optimistic lifetime estimates that emerge from pressure vessel data Glaser et al.(1984) recommend that the results for the 'worst' fibre spool be used in engineering design and also that lifetime estimates based on this data be considered as an upperbound, even when no hostile environmental effects are present. Since Parafil ropes, however, are fabricated from many different spools it is probable that the latter recommendation is not entirely applicable.

7.7.3 Relating the results for composite yarns to bare yarns.

Parafil consists of bare Kevlar 49 yarns and so the possible influence of the epoxy matrix used in the LLNL composites, on bare yarn stress-rupture behaviour, should be considered. The tensile strength

contribution of the epoxy matrix is likely to be small given the volume fraction of fibre in the composite (65% in the case of the pressure vessels) and the tensile properties of the epoxy resin. In fact, Chiao et al.(1976) used the Rule of Mixtures to estimate that the strength contribution of the epoxy in a Kevlar 49/epoxy yarn (fibre content 60% by volume) was less than two percent. It was also observed in this latter study that in uniaxial tension it was the failure of the Kevlar 49 fibre which caused catastrophic composite failure. Figure 7.7 indicates the stress-strain behaviour of the epoxy matrix used in the first test series at the LLNL (7.6) in comparison with composite behaviour. Certainly the data which was used to formulate the mathematical model of Glaser et al.(1984) came from tests in which all failures were fibre-controlled.

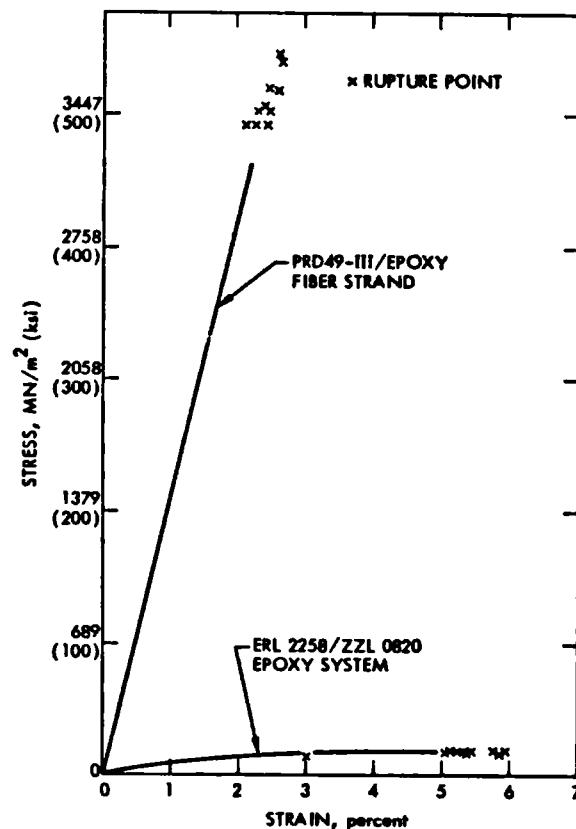


Figure 7.7 Typical stress-strain behaviour of epoxy resin in relation to composite yarns (Chiao et al.1974).

However, more conclusive evidence that the influence of the epoxy matrix on stress-rupture behaviour is small is provided by Cook et

al.(1982) and Howard and Parratt (1985). These researchers have investigated the stress-rupture behaviour of bare Kevlar 49 yarns at the Ministry of Defence, Propellants, Explosives and Rocket Motor Establishment (PERME). Figure 7.8 indicates the good agreement in tests at 110°C between the PERME bare yarn results and the LLNL impregnated yarn results, when the yarn stresses are expressed as a fraction of their respective ultimate tensile strengths. Since comparisons at ambient temperature are more relevant, it is encouraging to note that Howard and Parratt (1985) found that the curve for the most probable bare yarn lifetimes projected at ambient temperature (Figure 7.6) virtually coincided with the estimated 50% failure probability curve for the LLNL impregnated yarn pressure vessels made from the worst fibre spool. The latter observation not only provides further proof that the influence of the epoxy resin is small, but also that the LLNL mathematical model based on the worst fibre spool is the most pertinent.

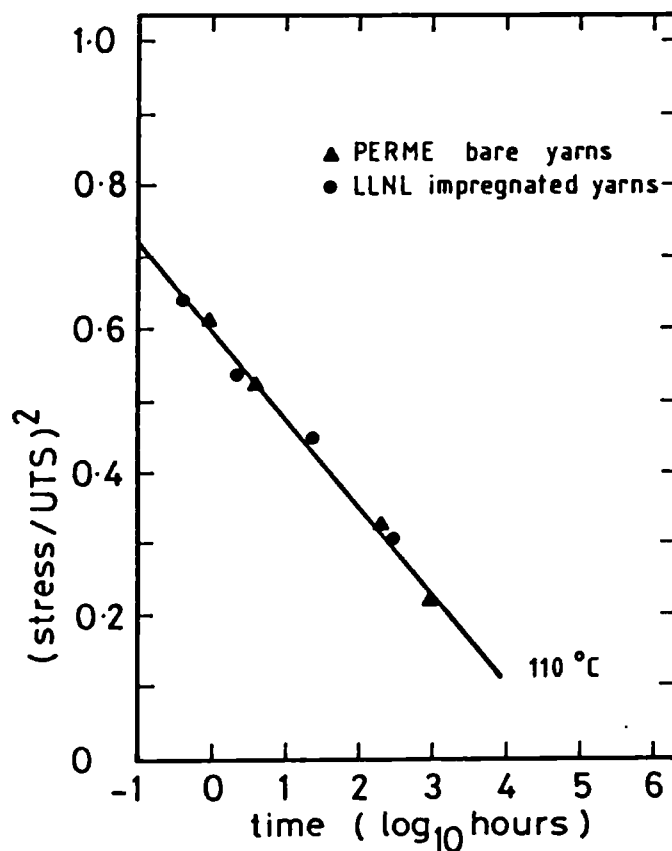


Figure 7.8 Stress-rupture behaviour of bare and impregnated Kevlar 49 yarns showing good agreement at 110°C (Cook et al.1982).

7.7.4 Relating the results for yarns to ropes

Considerable work has been conducted on modelling the static and time-dependent failure of fibre bundles based on the behaviour of single fibres. In particular, Phoenix (1976, 1978) actually predicted the theoretical static fatigue lifetimes of bundles of the LLNL impregnated Kevlar 49 yarns using the experimental data for single yarns. The results are shown in Figure 7.9. Phoenix (1978) found that when stress and log.time coordinates were chosen, the curve representing the mean time to failure for a large bundle (>50 yarns) was virtually parallel to that for the mean time to failure for a single yarn but displaced along the time scale by almost two decades. If the absolute stress on the bundle were reduced to about 90% of that on a single yarn then the respective mean lifetimes became approximately equal.

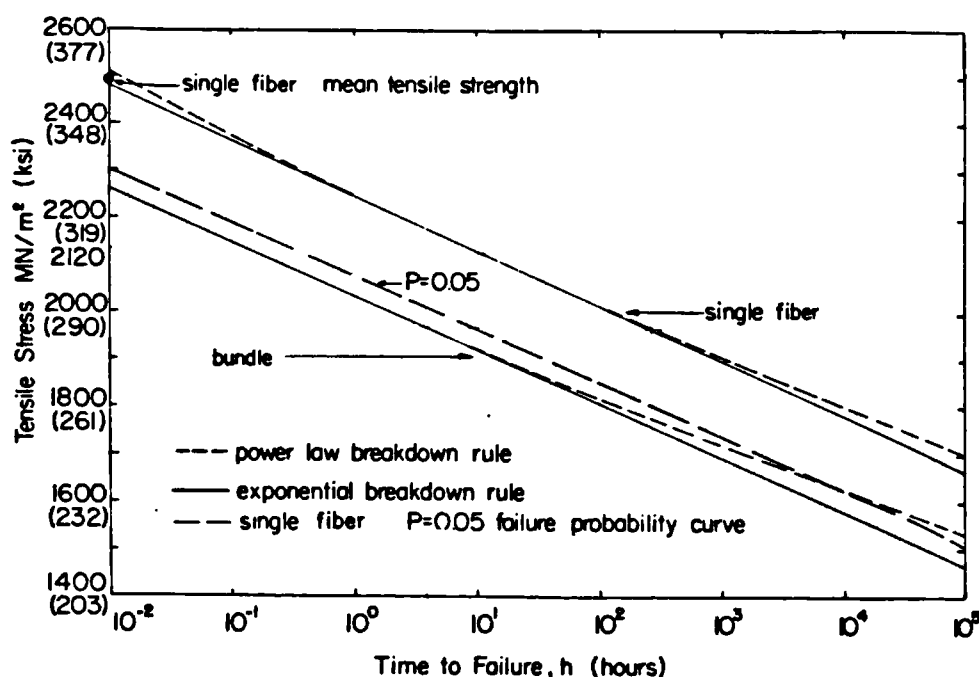


Figure 7.9 Comparison between theoretical stress-rupture behaviour of a bundle of impregnated Kevlar 49 yarns and experimental behaviour for individual yarns (Phoenix 1978).

Remarkably, the theoretical tensile strength efficiency (ratio of bundle tensile strength to the mean tensile strength of a single yarn) for a bundle of the LLNL impregnated yarns under the classic model (Phoenix 1976) also happens to be 90% (Chapter 8). Furthermore, parity between the bundle strength efficiency in static fatigue and the tensile strength efficiency has also been observed by Phoenix (1976) for yarns of different material.

It is assumed, therefore, that the stress-rupture results for bundles (ropes) can be predicted from the results for single yarns through application of the tensile strength efficiency factor. In practice, this can be achieved by normalising any lifetime equation involving absolute stress with respect to the ultimate tensile strength so that the lifetimes for a single yarn and a bundle, when stressed to the same percentage of their respective ultimate tensile strengths, become equal. It is for this reason that the test results for Parafil described here are related to nominal breaking load which was shown (Chapter 4) to adequately represent the true breaking strength of 60 tonne Type G Parafil ropes.

The connection between the two strength efficiencies as observed by Phoenix (1976) is perhaps not so surprising when tests conducted at high stresses are considered since these should closely resemble tension tests under monotonic loading. Of further interest, Phoenix (1976, 1978) concluded that the selection of the breakdown rule (e.g. exponential or power law) was not critical from a practical viewpoint and hence, that the argument of whether stress-rupture data should be plotted on stress versus log.time or log.stress versus log.time coordinates was really a minor one.

There is significant experimental evidence to support the theoretical observations of Phoenix (1976, 1978). Kenney et al.(1985) have conducted a series of tests to compare the behaviour of synthetic fibres, yarns and ropes when subjected to cyclic and static fatigue. The primary material studied was Du Pont 707 nylon in the form of monofilaments, 1260 denier yarns, and 3/16 inch diameter double-braided ropes.

The ultimate tensile strengths of the three structures decreased significantly with size owing to statistical bundle effects. However,

when the results for cyclic fatigue were normalised with respect to the relevant ultimate tensile strength, very good experimental agreement between the three structures was observed. More importantly, the agreement for normalised stress-rupture results from tests conducted using dead-loads and servohydraulic machines, was also good as indicated in Figure 7.10.

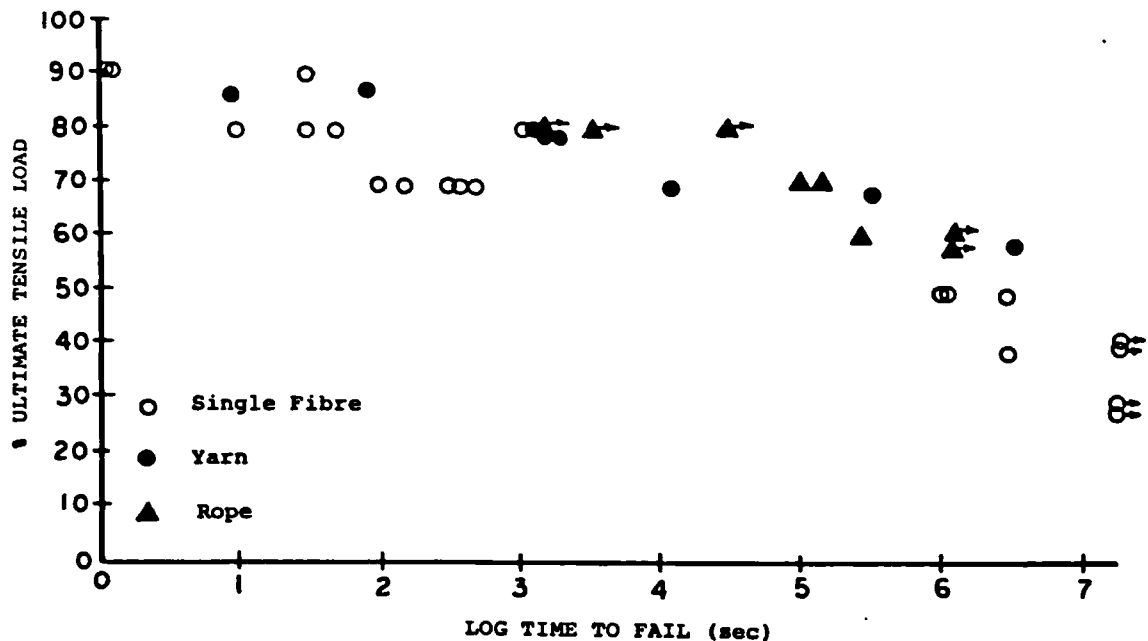


Figure 7.10 Comparison of stress-rupture behaviour for nylon fibres, yarns and ropes showing good agreement when results are normalised with respect to ultimate strength (Kenney et al.1985).

Kenney et al.(1985) also found that a simple creep rupture mechanism, based on reaction rate theory, was adequate for modelling the fatigue data. Reaction rate theory (e.g. Coleman and Knox 1957) predicts that cyclic-fatigue lifetimes are independent of the frequency of loading and that the criterion for fibre failure is the total time under load. While Lyons (1958) and Prevorsek and Lyons (1964) have questioned the applicability of Coleman's model in relation to cyclic fatigue, Kenney et al.(1985) observed that the behaviour of nylon, Kevlar 49 and polyester fibres all conform to the model. Figure 7.11 shows that there is also good agreement between the experimental cyclic fatigue data for Kevlar 49 and a prediction based on reaction rate theory.

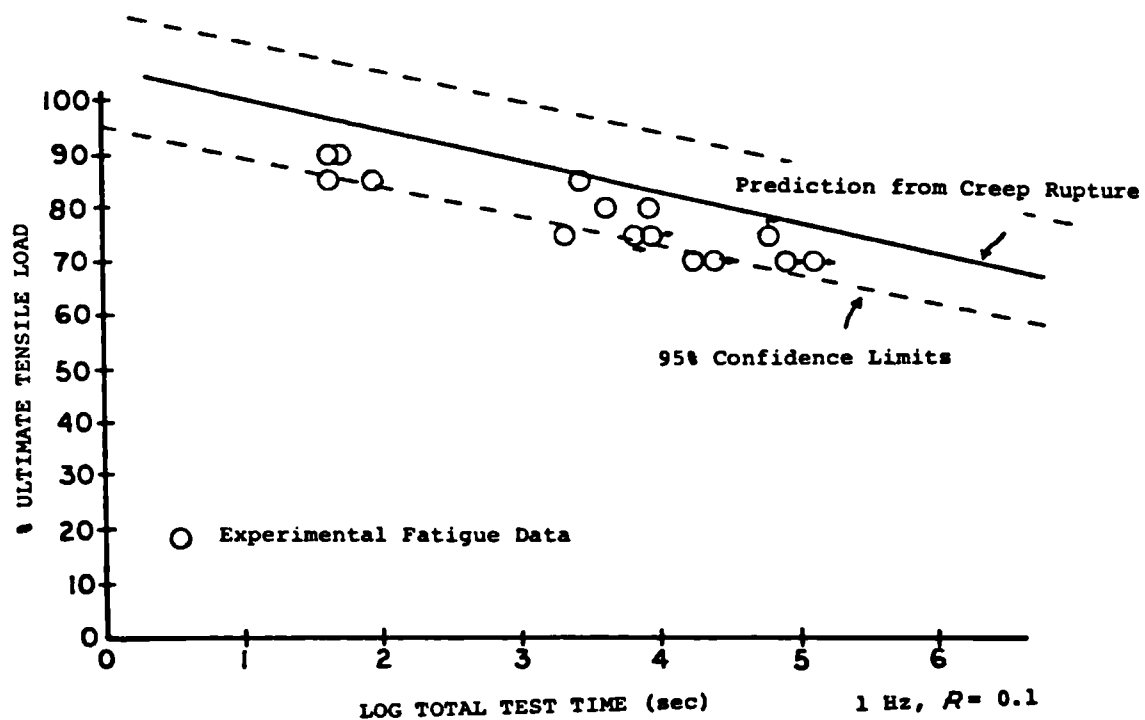


Figure 7.11 Comparison between Kevlar 49 cyclic fatigue data and a prediction based on reaction rate theory (Kenney et al.1985).

7.7.5 A comparison between Parafil stress-rupture data and the LLNL model.

Appendix 4 indicates how lifetime probability curves for Parafil Type G ropes may be constructed using the mathematical model for the LLNL Kevlar 49 composite yarns (Glaser et al.1984).

In using this model the following assumptions have been made:

- (1) pressure vessel tests are appropriate for determining the stress-rupture behaviour of yarns in essentially uniaxial strain fields (7.7.2)
- (2) the LLNL pressure vessel tests based on the worst fibre spool are the most relevant (7.7.2, 7.7.3)
- (3) the results for impregnated Kevlar 49 yarns apply equally to bare yarns (7.7.3)
- (4) the results for yarns may be related to ropes by normalising

with respect to ultimate tensile strength (7.7.4)

(5) there are no hostile environmental effects present.

Figure 7.12 indicates the fairly good agreement between the experimental stress-rupture data for Parafil Type G ropes and the LLNL mathematical model for pressure vessels based on the worst fibre spool. The majority of the results for Parafil fall between the 5% and the 95% probability of failure curves for the yarn (where 90% of all the data ought to lie).

The equations for the probability curves (Appendix 4) are non-linear. However, the non-linear term predominates at high stresses only, thus reflecting the associated increase in scatter with stress (Figure 7.12). At likely operating stresses ($\sigma/\sigma_{UTS} < 0.7$), however, the relationship between applied stress and the logarithm of time may be considered as linear. Such a relationship is not only consistent with the reaction rate approach to time-dependent failure but indicates that stress versus log-time coordinates are the most appropriate for plotting Kevlar 49 stress-rupture data.

7.8 The use of a cumulative damage rule to predict lifetimes under time-dependent loading conditions.

7.8.1 Introduction

The constant stress that, if applied, gave rise to an acceptable probability of failure at the end of the required structural design life could be adopted as the initial prestress for a Parafil Type G prestressing tendon. This approach would be very conservative and economically disadvantageous, however, since the stress within any prestressing tendon reduces significantly with time (5.2). The conservative approach is illustrated in Figure 7.13 where the inefficiency of assuming a time-independent constant stress for a Parafil tendon is apparent.

A cumulative damage technique has been developed, therefore, to determine the lifetime of a Parafil tendon when subjected to a time-varying loading history. The technique enables the initial prestress applied to Parafil tendons to be maximised.

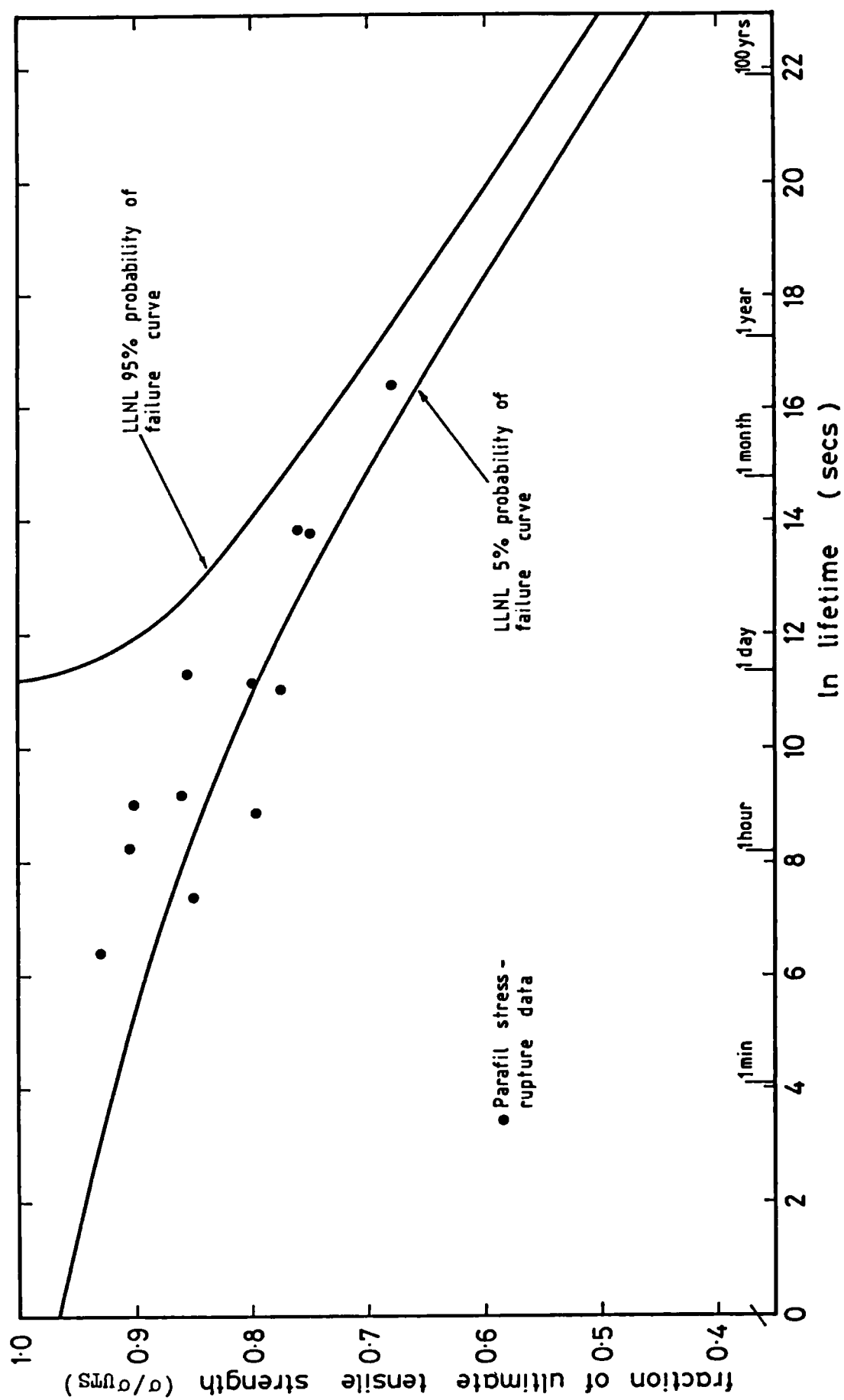


Figure 7.12 Comparison between Parafil stress-rupture data and the LLNL model.

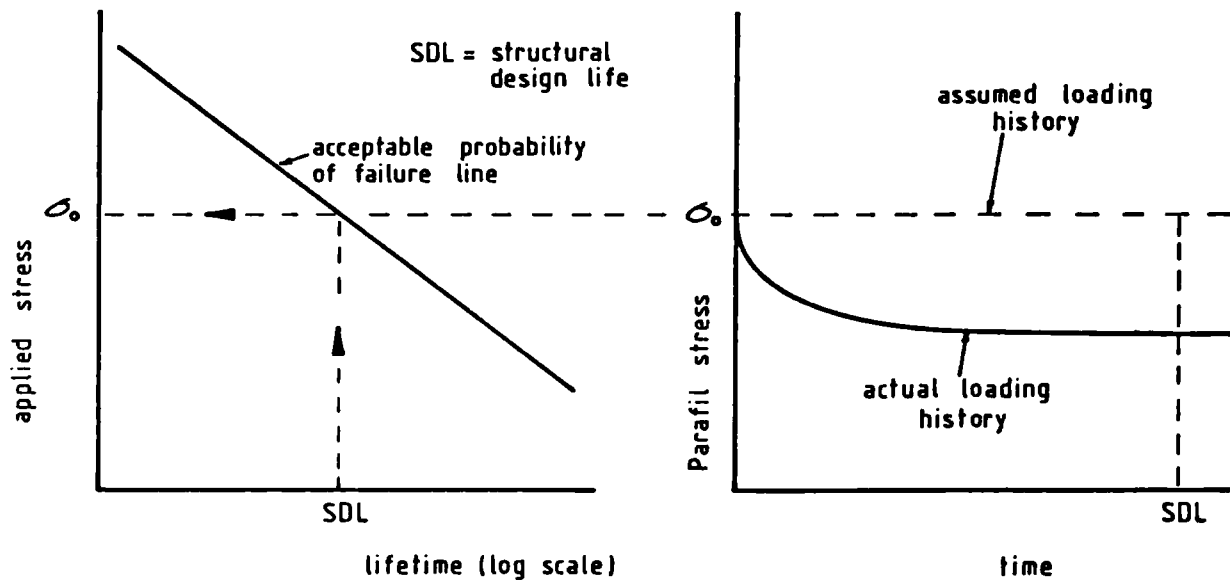


Figure 7.13 Conservative method of determining initial prestress for a Parafil Type G tendon.

7.8.2 The concept of cumulative damage.

It is widely accepted that most materials under stress accumulate damage with time. This has led to the formulation of cumulative damage laws based on the principle that when the damage reaches some constant critical value, failure occurs.

Bailey (1939) was probably the first to propose a cumulative damage law to compare the lifetimes of glasses under time-varying load conditions. The law assumed that the damage sustained at a particular stress level was equal to the time spent at that level divided by the lifetime corresponding to that stress. Failure was deemed to occur when the total damage reached unity; i.e. failure occurs when:

$$\int_0^{t_f} \frac{dt}{t_B [\sigma(t)]} = 1 \quad 7.8$$

where: t_f is the time to failure under a stress history $\sigma(t)$
 $t_B [\sigma]$ is the function which relates lifetime to stress in constant load experiments.

Bailey (1939) also expressed a specific cumulative damage law to cover cyclic loading which was based on the assumption that the number of loading cycles applied to a specimen, when expressed as a percentage of the number to failure at a given stress level, was the proportion of useful life expended. Failure was assumed to occur in fatigue when:

$$\sum \frac{X_i}{C_i} = 1 \quad 7.9$$

where: X_i is the number of cycles at stress level i
 C_i is the number of cycles to cause failure at the stress level i .

The fatigue law conflicts with Bailey's previous postulation which infers that fatigue failure is independent of the frequency of cycling and depends solely on the time spent under load.

Miner (1945) verified experimentally Bailey's fatigue damage law (Equation 7.9) in relation to metals. Indeed (and seemingly without justification) the law is often referred to as "Miner's fatigue rule".

More recently, Prevorsek (1966) used a cumulative damage law (Equation 7.8) to predict the influence of the rate of stressing on the tensile strength of polymeric threads. Similarly, de Silva (1980) used the law to predict whether failure would occur in the matrix or the fibre of metal composite fibres.

McKenna and Penn (1980) used a cumulative damage law to forecast the lifetimes of polymethylmethacrylate and polyethylene when subjected to monotonically increasing loads and also fatigue loadings. These researchers found that the lifetimes of these materials in constant load experiments could be described by the equation:

$$t_B(\sigma) = A e^{-B\sigma} \quad 7.10$$

Equation 7.10 conforms to the reaction rate models for creep failure (7.2 and 7.3) which predict a linear relationship between applied stress and the logarithm of time. Having determined the constants A and B experimentally, McKenna and Penn (1980) used Bailey's cumulative

damage law (Equation 7.8) to determine the theoretical lifetimes of these polymers under various load histories. The additivity of damage law was found to accurately predict lifetimes in tensile tests at varying stress-rates but was inadequate for describing the cyclic fatigue data. Perhaps Miner's fatigue rule would have been more appropriate for this loading case.

Hence, the use of a cumulative damage law for describing the behaviour of polymers when subjected to time-varying loads is not unprecedented; it has even been used for Kevlar 49. For instance, the stress-rupture results for bare Kevlar 49 yarns determined from tests conducted at PERME using a split-ring device (Cook and Howard 1982), required rationalisation since the applied stress was not constant. The problem with these tests was that, owing to the nature of the split-ring device, the yarn stress decayed with time (until creep failure occurred) yet it was desired to express the lifetime results as a function of constant stress. The use of Bailey's cumulative damage law enabled a greatly improved correlation between lifetime and applied stress to be determined.

7.8.3 Cumulative damage and absolute reaction rate theory.

It is not readily apparent that a cumulative damage technique to determine the lifetime of a Parafil Type G rope when subjected to a variable load history is entirely consistent with Coleman's absolute reaction rate approach to creep failure (Coleman and Knox 1957). It is shown in Appendix 5, however, that (for practical purposes) Bailey's cumulative damage law and Coleman's governing differential equation are mathematically the same. The Coleman model, therefore, provides further support that a polymer will fail when the cumulative damage it sustains, under any loading history, becomes equal to unity.

7.8.4 Cumulative damage and strength-retention.

Associated with the use of a cumulative damage law is the assumption that damage accumulates linearly with time. This assumption appears, however, to be opposed to the findings of Chiao et al.(1976) and Hamstad and Chiao (1975) thus raising (temporary) doubts about the

applicability of Bailey's cumulative damage law to Parafil. Later, however, valid explanations for the results of these researchers will be given.

Chiao et al.(1976) conducted a series of strength-retention tests on Kevlar 49/epoxy yarns which had spent a period of time under constant load. Eight sustained load levels were investigated ranging from 40% to 80% of the ultimate. Tensile tests were conducted after 100, 360 and 1000 hours. From 116 specimens subjected to dead-loading, 5 failed in stress-rupture (3 at 80% and 2 at 75% of the ultimate) while the surviving yarns displayed an average tensile strength retention of 96% of the ultimate; lower strengths had clearly been expected. The yarn results are consistent with the results of the strength-retention tests conducted on Parafil at the end of the stress-relaxation investigation (Table 5.4). Hence, questions regarding the accumulation of damage in Parafil are equally as pertinent.

Chiao et al.(1976) concluded that there was no correlation between strength-retention and stress-rupture lifetime and that 100% strength-retention was not indicative of the remaining life of the specimen. The use of reliable stress-rupture data for engineering design was recommended.

The same conclusion was drawn by Robinson and Schenectady (1952) in relation to steels tested at elevated temperature (the problem of stress-rupture in steels is non-existent at ambient temperature). These authors used a cumulative damage approach to determine the factor of safety for structural members under varying temperature and stress conditions. However, it was noted that low alloy steels displayed almost 100% strength retention after having spent a considerable period of time under stress at high temperature (e.g. 7600 hours at 260 MPa and 535°C). The authors remarked that the matter of significance was the impending creep failure at low stress and not that the short-term properties of specimens removed from the dead-loading apparatus remained largely unaffected.

Similarly, the results of work conducted by Hamstad and Chiao (1975) seem contrary to the idea of damage accumulating linearly with time. These investigators monitored the acoustic emission from a set of Kevlar 49 impregnated yarns subjected to a dead load of 88% of the

ultimate (Figure 7.14). The count summation did not build-up uniformly as expected but was found to increase rapidly just prior to failure which perhaps indicates a sudden loss of strength.

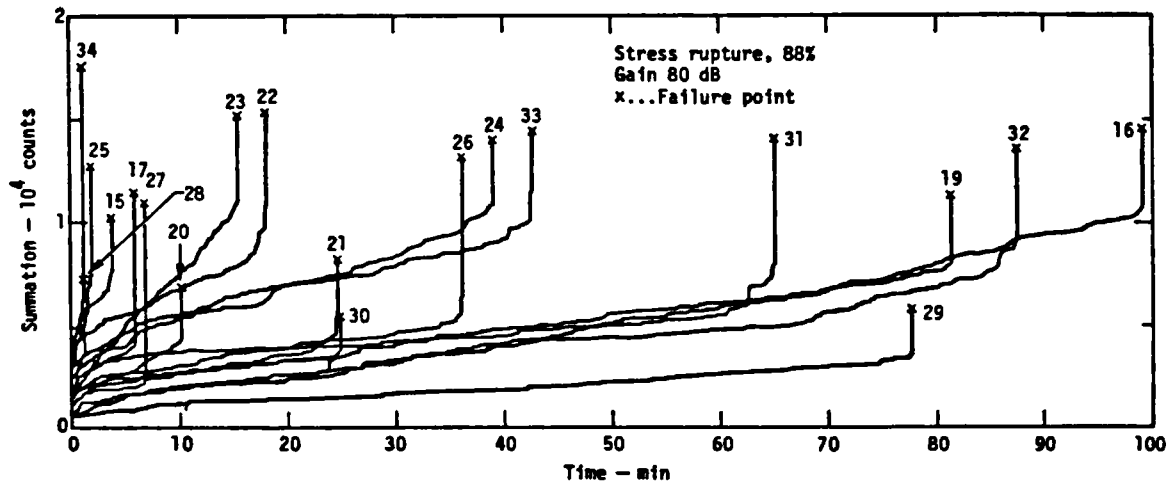


Figure 7.14 Acoustic emission during stress-rupture tests on Kevlar 49 impregnated yarns (Hamstad and Chiao 1975).

A rational (but incorrect) conclusion that could be drawn from the strength-retention tests (on both Kevlar 49 composite yarns and Parafil) and also from the acoustic emission tests is that the accumulation of damage with time due to stress is non-linear but builds-up rapidly just prior to failure. However, despite this seemingly rational conclusion, Coleman's absolute reaction rate theory (Coleman and Knox 1957) which is largely synonymous with the linear additivity of damage rule (Appendix 5) predicts that the tensile strength of Kevlar 49 would indeed remain high until just prior to the occurrence of creep failure.

For example, from the Coleman model it can be shown (using Equation 7.3) that for constant load experiments:

$$\Delta\sigma = - \frac{1}{B} \ln (1 - t/t_B) \quad 7.11$$

where $\Delta\sigma$ is the loss in tensile strength

B is a constant derived from dead-loading experiments

t is the time under stress

t_B is the lifetime.

From Equations A4.8 and A4.9 (Appendix 4) the constant B (for the LLNL pressure vessels) may be approximated by $31.12/\sigma_{UTS}$, if the non-linear term, predominant at high stresses only, is ignored. Hence, the strength losses for the LLNL Kevlar 49 composite yarns (and perhaps Parafil Type G ropes also) may be determined; the losses remain small until just prior to creep failure as shown in Table 7.1. Zimmerman (1974) observed very similar strength reductions in nylon, polyester and rayon fibres and also in the aramid fibre, Nomex.

Chiao et al.(1976) believed that the near 100% strength-retention observed in Kevlar 49/epoxy yarns could be explained by Zhurkov's flaw-growth model for fibre failure (7.5). Christensen (1981) has since confirmed that a kinetic theory of crack growth can explain a sudden reduction in strength prior to failure. Figure 7.15 indicates good agreement between the theoretical prediction for residual strength (strength-retention) based on this model and experimental data. The data is from tests conducted on Kevlar 49/epoxy yarns at 100°C subject to a load equal to 56% of the ultimate.

Christensen (1981) showed that the residual strength of polymers depended on the reciprocal of the square root of the crack size. Since the crack growth was assumed to be very slow until a critical size was reached, high residual strengths were to be expected. Hence, in agreement with the absolute reaction rate theory, flaw-growth theory also indicates that high residual strengths are not inconsistent with Bailey's law of cumulative damage.

time under load/lifetime	% strength loss
0.8	5.2
0.9	7.4
0.99	14.8
0.999	22.2

Table 7.1 Theoretical tensile strength losses for Kevlar 49 in constant load tests.

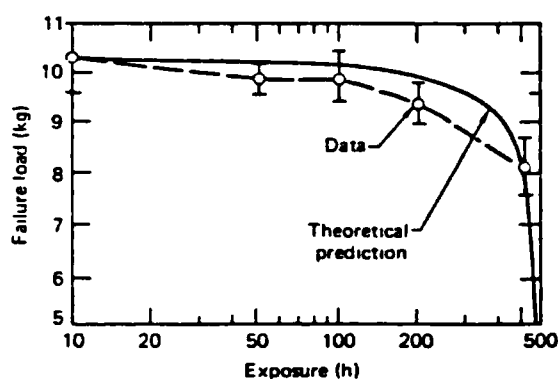


Figure 7.15 Comparison between strength-retention data for Kevlar 49 impregnated yarns and a prediction based on flaw-growth theory (Christensen 1981).

7.8.5 Conclusion

Explanations for the development of damage in polymers remain controversial. In favour of the idea of uniform damage accumulation, however, Howard and Parratt (1985) concluded that the failure of Kevlar 49 yarns was the consequence of a series of small damage events. Similarly, Christensen and Glaser (1983) suggest that even though many polymers in creep tests appear to be unchanging both in visual appearance and in dimensions, internal micro-scale mechanisms of damage growth are continually occurring which ultimately lead to failure.

Furthermore, the use of a cumulative damage law to predict lifetimes in polymers subject to varying load states is not unprecedented (7.8.2.). There appears to be a strong argument, therefore, for the adoption of a model based on a steady or linear accumulation of damage, to predict the lifetime of Parafil Type G ropes when subject to the time-varying stress-state of a prestressing tendon.

7.9 Definition of the loading history for a Parafil prestressing tendon

7.9.1 Introduction

If the magnitude of the initial prestress applied to a Parafil rope, when used as a prestressing tendon, is to be maximised, then the loading history for the tendon over its entire structural design life must be defined. The loading history should account for all the prestress losses that can occur. For unbonded tendons in post-tensioned concrete these losses may be assessed by considering:

- (1) stress-relaxation of the tendon
- (2) concrete shrinkage
- (3) concrete creep
- (4) elastic deformation of the concrete
- (5) anchorage slip
- (6) friction between the tendon and the prestressing duct.

7.9.2 Stress-relaxation

Table 5.3 indicates that the long-term stress-relaxation for Parafil Type G ropes is only marginally dependent on stress. Furthermore, preliminary investigations indicate that an acceptable initial value for the prestress in a Parafil tendon is likely to be approximately equivalent to 50% of the ultimate tensile strength, decaying to around 40% over a period of years. Over this small range, and given the assumptions on which the long-term stress-rupture of Parafil is based, relaxation may be regarded as essentially stress-independent. Using Equation 5.2, therefore, stress-relaxation as a function of time may be expressed as:

$$r = 3.63 + 0.67 \log (t - 100) \quad t > 100 \quad 7.12$$

where: r is the stress-relaxation as a % of the nominal breaking load (% NBL)
 t is the time in hours.

In terms of stress, the loss of stress due to relaxation is given by:

$$dS_{\text{relax}} = 70.0 + 12.9 \log (t - 100) \quad t > 100$$

For $t \gg 100$, as is typical in prestressed concrete, this can be approximated by:

$$dS_{\text{relax}} = 70.0 + 12.9 \log t \quad 7.13$$

where: dS_{relax} is the loss of stress due to tendon relaxation (MPa)
 t is the time in hours.

7.9.3 Concrete shrinkage

Concrete shrinkage strains decrease as the relative humidity of the environment increases. Table 7.2 gives typical concrete shrinkage strains for a post-tensioned member in which the transfer of prestress takes place between 7 and 14 days after concreting (CP110 1972, Abeles and Bardhan-Roy 1981). The loss of prestress due to concrete shrinkage may be estimated by multiplying the shrinkage strain (Table 7.2) by the elastic modulus for Parafil Type G ropes (1.18×10^5 MPa).

Figure 7.16 shows how concrete shrinkage strain typically varies with time. A linear dependency of shrinkage strain with the logarithm of time would describe the behaviour adequately. If it is assumed that the shrinkage strains indicated in Table 7.2 correspond to a 20 year time period (little shrinkage occurs after this time) then for the case of a humid environment:

$$\begin{aligned} dS_{\text{shrinkage}} &= \frac{8.3}{5.24} \log t \quad \{\log (20 \text{ year hours}) = 5.24\} \\ &= 1.6 \log t \quad \text{'humid'} \end{aligned} \quad 7.14$$

where: $dS_{\text{shrinkage}}$ is the loss of stress due to concrete shrinkage (MPa)
 t is the time in hours.

Similarly, for a normal exposure:

$$dS_{\text{shrinkage}} = 4.5 \log t \quad \text{'normal'} \quad 7.15$$

Environment	Shrinkage strain	Loss of prestress (MPa)
humid (90% relative humidity)	70×10^{-6}	8.3
normal (70% relative humidity)	200×10^{-6}	23.6
dry	270×10^{-6}	31.9

Table 7.2 Typical concrete shrinkage strains and associated prestress losses in a Parafil Type G tendon.

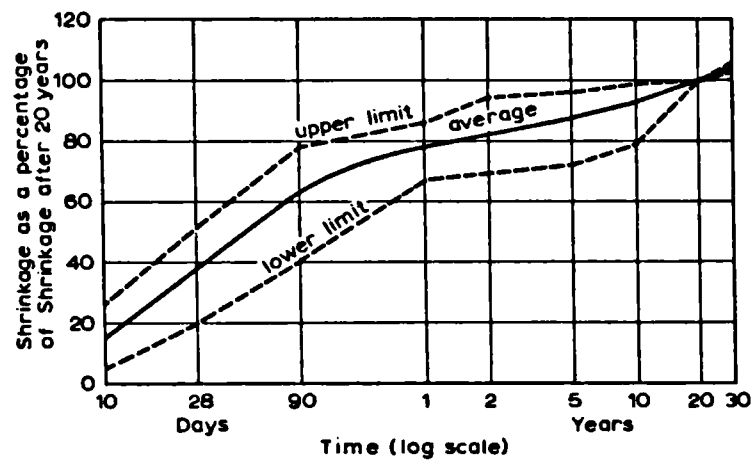


Figure 7.16 The dependency of concrete shrinkage on time (Neville 1981).

and for a dry exposure:

$$\Delta S_{\text{shrinkage}} = 6.1 \log t \quad \text{'dry'} \quad 7.16$$

A logarithmic function is largely consistent with the observations that half of the total shrinkage occurs during the first month and three-quarters occurs in the first six months after transfer (CP110 1972).

7.9.4 Concrete creep

The loss of prestress due to concrete creep is dependent on the ratio of the compressive stress in the concrete to the concrete strength. For concrete stresses less than one third of the cube strength at transfer creep is usually considered to be proportional to the stress in the concrete (CP110 1972). For post-tensioned members, in which the prestress is transferred between 7 and 14 days after concreting, a creep strain of 36×10^{-6} per MPa is recommended by British Codes of Practice. For compressive stresses in excess of one-third of the transfer cube strength or for transfer cube strengths less than 40 MPa, the use of higher creep strain values are recommended when assessing prestress losses.

The prestress loss due to concrete creep (ΔP_{creep}) is approximately given by:

$$\Delta P_{\text{creep}} = E \times \epsilon_c \times f_c \quad 7.17$$

where: E is the elastic modulus of the tendon
 ϵ_c is the creep strain per unit of concrete stress
 f_c is the concrete stress at the level of the tendon at transfer.

Consider three categories of prestress loss due to concrete creep: minimum, typical and maximum.

A Minimum creep

For realistic applications the concrete stress at tendon level is unlikely to be less than 7 MPa. Hence, the minimum prestress loss due

to creep (assuming the cube strength at transfer exceeds 40 MPa) is given by:

$$\begin{aligned} dP_{\text{creep}} &= 1.18 \times 10^5 \times 36 \times 10^{-6} \times 7 \\ &= 29.7 \text{ MPa.} \end{aligned}$$

There are numerous equations representing concrete creep as a function of time but once again a linear relationship with the logarithm of time seems adequate as indicated in Figure 7.17. If the prestress loss due to creep is considered to apply to a twenty year period then:

$$\begin{aligned} dS_{\text{creep}} &= \frac{29.7}{5.24} \log t \\ &= 5.7 \log t \quad \text{"minimum"} \quad 7.18 \end{aligned}$$

It can easily be checked that Equation 7.18 largely agrees with the following observations:

- (1) half the total creep occurs in the first month after transfer (CP110 1972)
- (2) three-quarters of the total creep occurs in the first six months after transfer (CP110 1972)
- (3) the creep after 20 years is between 1.5 and 3.0 times the creep after 2.5 months (Neville 1981).

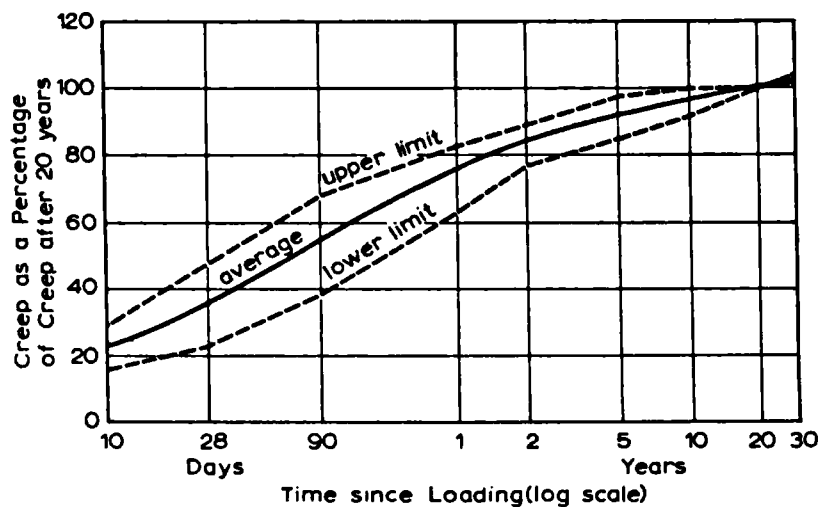


Figure 7.17 The dependency of concrete creep on time (Neville 1981).

B Typical creep

Usually the cube strength at transfer (f_{ct}) is approximately 40 MPa and the concrete stress at the tendon level is about $1/3 f_{ct}$. Hence, assuming $f_{ct} = 40$ and $f_c = 13$, a prestress loss of 55.2 MPa would occur (Equation 7.17). For a typical case, therefore:

$$dS_{\text{creep}} = 10.5 \log t \quad \text{"typical"} \quad 7.19$$

C Maximum creep

High creep strains would occur if the concrete stress at the tendon level were high, or if the concrete strength at transfer were low. The maximum concrete stress at the level of the tendon would probably not exceed 22 MPa (Abeles and Bardhan-Roy 1981). Assuming this stress was equal to one half of the cube strength at transfer, a prestress loss of 117 MPa would occur. A slightly smaller loss would occur if the cube strength at transfer were considered to be 30 MPa (about the practical minimum) and if the concrete stress at tendon level were 15 MPa. Therefore, for the case of maximum creep:

$$dS_{\text{creep}} = 22.3 \log t \quad \text{"maximum"} \quad 7.20$$

7.9.5 Elastic deformation of the concrete

For prestressed concrete members incorporating a single tendon, elastic deformation of the concrete will not cause a reduction in the applied prestress, since the deformation will have occurred before the tendon is anchored. This is not so for members containing multiple tendons, however, since tendons that have been stressed and anchored will be affected by subsequent stressing operations. In this situation, it is common when steel tendons are used, to overstress the tendons to compensate for elastic deformation of the concrete.

It would be unwise, however, to overstress Parafil Type G tendons (above the safe operating stress levels determined later) in this manner since this could lead to an unwelcome accumulation of stress-damage thus affecting the future integrity of the member. It could be

argued that any overstress would be temporary but this could not be guaranteed. An alternative, however, might be to apply the stress to each Parafil tendon in a series of increments. In this manner, the losses of prestress due to elastic deformation of the concrete would be largely eliminated and the Parafil tendons would be most efficiently employed without suffering additional stress-damage.

7.9.6 Anchorage slip and friction

Losses due to anchorage slip are associated with the "pull-in" of wedges into sockets in conventional steel prestressing systems. Since a prestressing system based on threaded connections has been developed for Parafil ropes, it is no longer necessary to consider this type of loss.

In post-tensioned members incorporating steel tendons, friction between the tendon and the prestressing duct causes a variation in the degree of prestress along the length of the member. The stress applied at the anchorage point is generally increased to account for the loss of prestress due to friction at the critical section of the member. The force in the tendon, therefore, is higher at the stressing point than elsewhere.

It would not be sensible, however, to compensate for frictional losses at a critical section of a member incorporating a Parafil tendon by increasing the stress at the anchorage point (above the safe operating level) since this could lead to premature stress-rupture failure. However, as for elastic deformation losses, it would be necessary in design to determine the loss of prestress due to friction in order to calculate the stress conditions at critical sections of the member.

7.9.7 Summation of the time-dependent losses

In order to study the effects of varying degrees of prestress loss on the allowable initial prestress in a Parafil tendon, three separate loading histories will be considered. These loading histories will account for the minimum, typical and the maximum time-dependent losses that could reasonably be expected. Table 7.3 indicates the components of the three prestress loss classifications. In addition to these

classifications particular consideration will be given to three commonly adopted structural design lives (20, 50, 100 years) as described in Table 7.4 although general results will also be obtained.

Prestress losses considered	Classification
stress-relaxation (Eqn. 7.13) concrete shrinkage in a humid environment (Eqn. 7.14) minimum creep (Eqn. 7.18)	Minimum (A)
stress-relaxation (Eqn. 7.13) concrete shrinkage in a normal environment (Eqn. 7.15) typical creep (Eqn. 7.19)	Typical (B)
stress-relaxation (Eqn. 7.13) concrete shrinkage in a dry environment (Eqn. 7.16) maximum creep (Eqn. 7.20)	Maximum (C)

Table 7.3 Classifications of prestress losses.

Structural design life (years)	Typical application
20	offshore platform nuclear power station
50	building structures
100	bridges prestigious structures

Table 7.4 Design lives and typical applications.

If the equations relevant to each classification of prestress loss are combined then the total time dependent losses at time t are given by:

$$dS_A = 70.0 + 20.2 \log t \quad 7.21$$

$$dS_B = 70.0 + 27.9 \log t \quad 7.22$$

$$dS_C = 70.0 + 41.3 \log t \quad 7.23$$

where: dS is the prestress loss (MPa)
 A, B, C , correspond to the classification in Table 7.3
 t is the time in hours.

If Equations 7.21, 7.22 and 7.23 are expressed in terms of percentages of the nominal breaking load (% NBL) then the load in a Parafil Type G tendon at time t for each classification is given by:

$$f_A(t) = f_{A \max} - [3.63 + 1.05 \log t] \quad 7.24$$

$$f_B(t) = f_{B \max} - [3.63 + 1.45 \log t] \quad 7.25$$

$$f_C(t) = f_{C \max} - [3.63 + 2.14 \log t] \quad 7.26$$

where: $f(t)$ is the load in a Parafil tendon at time t expressed as a % NBL.
 $f_{\max}$ is the allowable maximum initial prestress as a % NBL
 t is the time in hours
 A, B, C , correspond to the classification in Table 7.3.

7.10 An acceptable probability of failure

For the majority of concrete structures, a socially acceptable probability of failure is 1 in 10^6 (Kong and Evans 1980). The estimation of very low probabilities of failure for Parafil ropes when subjected to constant loads, based on the mathematical model of Glaser et al.(1984) is sensitive to the Weibull and polynomial assumptions (7.6). Thus, Glaser et al.(1984) discourage the use of the model to predict failure probabilities below 10^{-3} even though 10^{-6} probability curves based on the LLNL data do appear in the literature (Figure 7.5).

In the determination of acceptable values for the initial prestress for Parafil there appears to be no sensible alternative to adopting the 10^{-6} probability of failure curve, despite its limitations. An irrational alternative would be to apply a factor of safety to the more confident 10^{-3} probability of failure curve; if the combination of a safety factor in conjunction with the latter curve were to lead to an acceptable probability of failure, then the safety factor itself, would need to be statistically-based. The relationship between applied stress and the logarithm of time, to represent a probability of failure in a Parafil Type G rope of 10^{-6} , has been determined therefore, and is quoted in Appendix 4 (Equation A4.10).

According to theory governing the statistics of bundles the scatter of lifetime results for a bundle containing 1000 Kevlar 49/epoxy yarns ought to be an order of magnitude less than the scatter for single yarns (Phoenix 1978). In this respect forecasts for Parafil Type G ropes based on the LLNL data should be conservative. The results gathered so far from stress-rupture tests conducted on 60 tonne ropes (each rope contains well in excess of 1000 yarns) do not appear to confirm that predictions will be a lower bound (Figure 7.12); however, at this stage, it is difficult to draw any firm conclusions about variability, since the amount of data is limited.

7.11 Computer program

A computer program has been written to determine suitable values of the initial prestress for a Parafil tendon, as a function of the structural design life, based on the three loading histories described earlier.

The following assumptions have been made in formulating the program:

- (1) the LLNL model adequately describes the stress-rupture behaviour of Parafil Type G ropes (7.7)
- (2) the 10^{-6} probability of failure curve is sufficiently accurate to enable acceptable values for the initial prestress to be determined (7.10).
- (3) Bailey's cumulative damage law is appropriate for determining the lifetime of a Parafil Type G rope when subject to a variable loading history (7.8).

Initial values for the tendon prestress are determined in the program by:

(1) dividing the loading history into a series of time increments.

(2) assessing the damage sustained at each stress level, over each time increment, from the stress-lifetime equation (Equation A4.10).

(3) summing the damage; a probability of failure of 1×10^{-6} is deemed to be reached when the sum of the damage reaches unity.

An examination of Equations 7.21, 7.22 and 7.23 reveals that 74% to 85% of the final prestress loss in a Parafil tendon occurs within the first year and so it is important that the time increments for this period are kept relatively short (say 7 days) if sensible results are to be obtained. Figure 7.18 illustrates how the cumulative damage iterative procedure operates in the computer program.

For the first time increment considered the load is conservatively assumed to be equal to the initial maximum value. A listing of the computer program called DAM6, and some typical output is shown in Appendix 6.

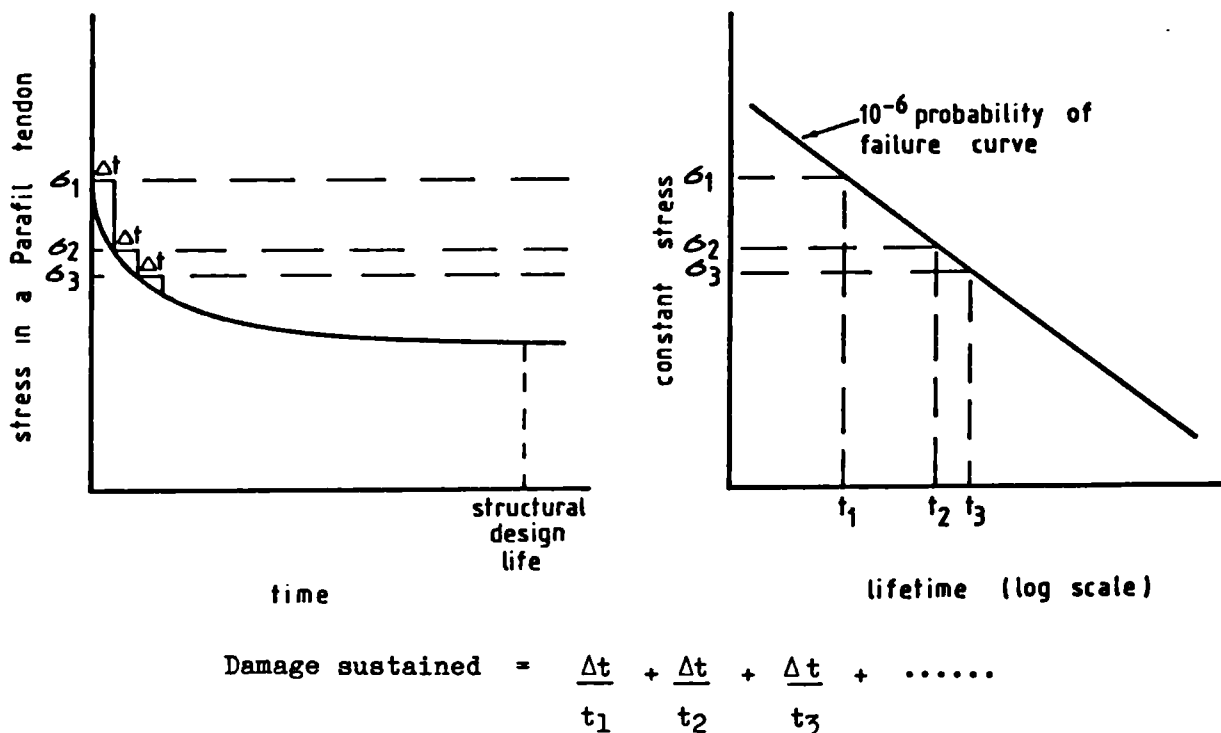


Figure 7.18 Representation of cumulative damage iterative procedure.

7.12 Results and discussion

Figure 7.19 indicates the stress that may be safely applied to a Parafil Type G rope (when used as a tendon in prestressed concrete) depending on the chosen structural design life and the amount of prestress losses that are expected. The probability of failure associated with the stresses shown in Figure 7.19 is 1 in 10^6 ; hence, no adjustments (through the use of safety factors for instance) to prestress values taken from Figure 7.19 need to be made.

Values for the magnitude of the initial prestress, taken from Figure 7.19, for commonly chosen structural design lives, are shown in Table 7.5. For a 100 year design life with only small prestress losses a stress equivalent to 47% NBL may be applied, whereas, for a 20 year design life and high losses, the initial prestress may be increased to 56% NBL. The constant loads that would also result in a 10^{-6} probability of failure after 20, 50 and 100 years are shown in Table 7.6. The magnitude of the initial prestress that may be safely applied to a Parafil Type G tendon, when its loading history is taken into account, far exceeds that based on the extremely conservative constant loading case. The gains, ranging from 8% to 15% NBL (Table 7.6) will result in considerable economic benefits.

Table 7.7 indicates the magnitude of the effective or residual prestresses corresponding to the maximised initial values. Prestress losses arising from elastic deformation (7.9.5) or friction (7.9.6) if present, should be subtracted from the values given in Table 7.7. If these two causes of prestress loss are ignored, it is apparent, that despite the limitations imposed by the stress-rupture phenomenon, the effective prestress in a Parafil Type G tendon remains large (700 to 800 MPa). Comparative values for steel prestressing strand and alloy bar (also ignoring elastic deformation and friction) are typically 1000 MPa and 600 MPa respectively.

It is interesting to note (Table 7.7) that for a given structural design life the effective prestress is virtually independent of the size of the prestress loss. For example for design lives of 20, 50 and 100 years the effective prestress would be approximately 800, 750 and 700 MPa respectively independent of the magnitude of the time-dependent losses. It may be possible to take advantage of this relationship in the design process.

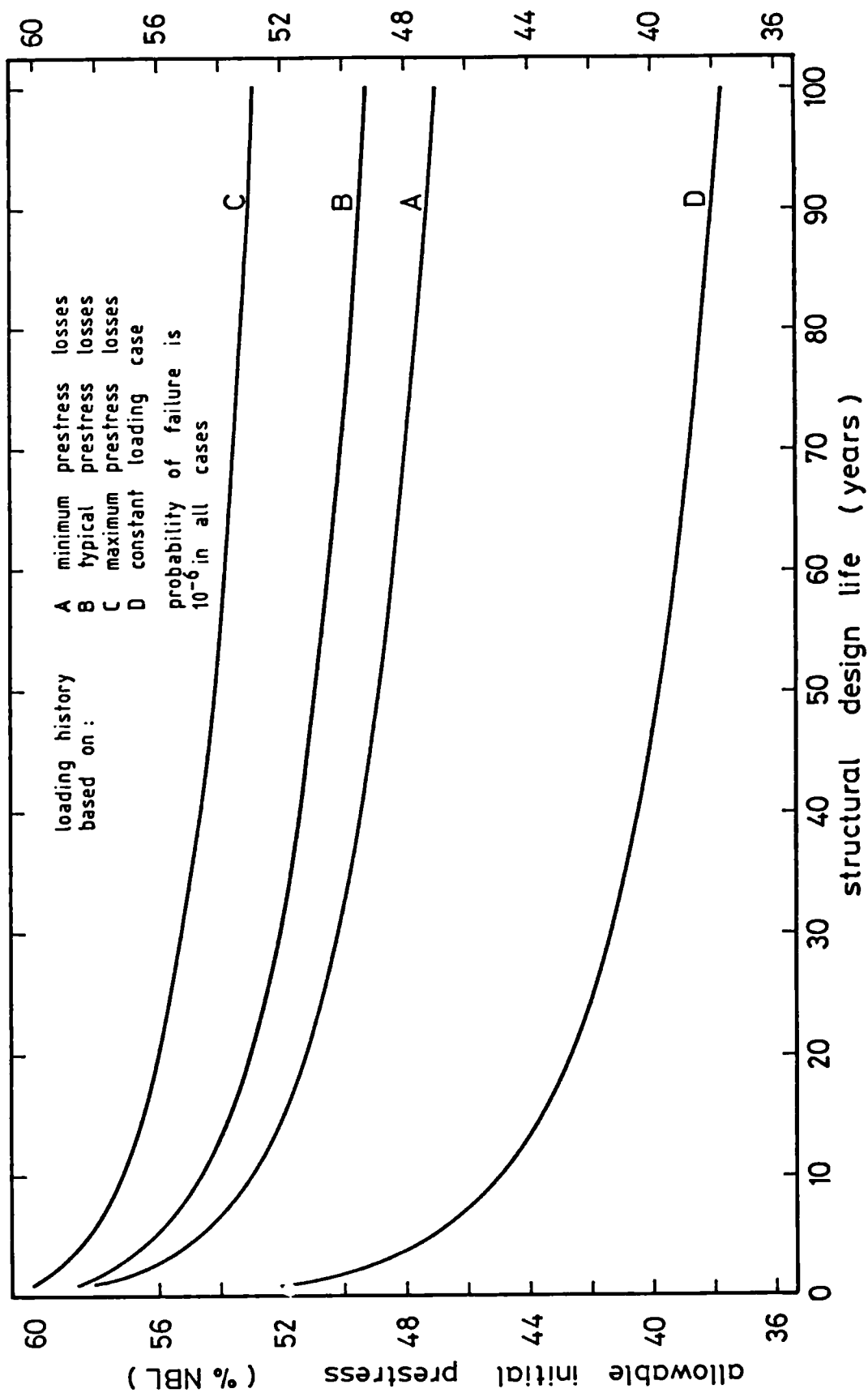


Figure 7.19 Plot of allowable initial prestress against structural design life for various loading histories.

Prestress losses considered	General prestress loss classification	Initial prestresss (% NBL)		
		Structural design life (years)		
		20	50	100
stress-relaxation concrete shrinkage in a humid envt. minimum creep	minimum	51	49	47
stress-relaxation concrete shrinkage in a normal envt. typical creep	typical	53	51	49
stress-relaxation concrete shrinkage in a dry envt. maximum creep	maximum	56	54	53
constant load (no losses)		43	40	38

Table 7.5 Safe values for the initial prestress of a Parafil Type G tendon.

Structural design life (years)	Constant load to give 10^{-6} prob. of failure (% NBL)	Advantage in using cumulative damage approach (% NBL)		
		minimum	typical	maximum
20	43	8	10	13
50	40	9	11	14
100	38	9	11	15

Table 7.6 Benefits of using cumulative damage approach.

Prestress losses considered	Allowable initial prestress (% NBL)	Prestress loss (% NBL)	Effective prestress (% NBL)(MPa)		Design Life (years)
minimum	51	9	42	810	20
typical	53	11	42	810	
maximum	56	15	41	790	
minimum	49	9.5	39.5	760	50
typical	51	12	39	750	
maximum	54	15.5	38.5	740	
minimum	47	10	37	710	100
typical	49	12	37	710	
maximum	53	16.5	36.5	700	

Table 7.7 Effective prestresses for Parafil Type G tendons.

7.13 Temperature effects

7.13.1 Due to temperature changes

The coefficients of thermal expansion for concrete and steel are very similar; an average value for concrete is typically $12 \times 10^{-6}/^{\circ}\text{C}$ while that for steel is about $11 \times 10^{-6}/^{\circ}\text{C}$. Consequently, there is little fluctuation in the magnitude of the prestress in a steel tendon due to temperature changes, since the two materials can expand and contract equally.

The coefficient of longitudinal thermal expansion for Kevlar 49 at room temperature is approximately $-2 \times 10^{-6}/^{\circ}\text{C}$ (Du Pont 1981). Initial investigations reveal that the coefficient is likely to remain negative even when Kevlar 49 is subjected to tensile stress (Guimares 1985). Hence fluctuations of prestress in Parafil tendons are bound to occur.

The effects of temperature changes, as typically experienced in the British Isles, on the stress in a Parafil tendon, are considered in Appendix 7. If the permissible temperature at which a Parafil tendon can be stressed and anchored during construction is limited to the range $+4^{\circ}\text{C}$ to $+23^{\circ}\text{C}$, then the prestress fluctuation can be restricted to between $+3\%$ NBL and -4% NBL.

A permanent stress increase of $+3\%$ NBL would have serious detrimental effects on the life of Parafil tendons (see Table 7.5 for example). However, for tendons stressed on-site at 4°C (in the British Isles) the increase in stress averaged over the year would only be about $+1\%$ NBL. This possible increase could be taken into consideration during design by simply reducing the magnitude of the allowable prestress, as shown in Table 7.5 by a corresponding 1% NBL.

The effect of a prestress fluctuation (due to temperature) on the stresses within the concrete should also be considered. Generally, by inspection of stress distributions within typical prestressed concrete members, any changes in concrete stresses will be limited to about ± 2 MPa. An increase in the concrete compressive stress of this amount could probably be tolerated. However, an increase of 2MPa in the tensile stress could lead to cracking of the concrete. In this case,

a reduction in the allowable tensile stress, to compensate for possible concrete stress fluctuations, is advisable.

7.13.2 Due to excessive air temperatures

The stress-rupture lifetimes of most polymers reduce with increasing temperature largely in accordance with the Arrhenius equation (Equation 7.6). Kevlar 49 offers no exception to this mode of behaviour as illustrated in Figure 7.6.

The LLNL pressure vessel tests were conducted at $27.7 \pm 1.6^{\circ}\text{C}$. This temperature is greater than the mean air temperature of most developed countries. Hence, the detrimental effects of excessive temperatures will not have to be considered for most applications. Indeed, in the case of applications of Parafil within the British Isles the use of the LLNL data may afford a degree of conservatism.

There are some parts of the world, however, and also some specialist areas of application, where the mean temperature may exceed that at which the LLNL tests were conducted. In these few circumstances, a reduction in the magnitude of the operating stresses is advisable.

Glaser et al.(1984) have constructed a mathematical model (similar to that for pressure vessels) to describe the stress-rupture behaviour of Kevlar 49/epoxy yarns at elevated temperatures. The model is based on tests of short duration (22 days maximum) conducted within a very narrow temperature range (100°C to 120°C). For these reasons, Glaser et al.(1984) recommend that this model is not used for predicting stress-rupture behaviour at ambient temperature. However, despite the potential inaccuracy, the model indicates that in the range 28°C to 40°C , and for typical structural design lives (greater than 20 years) the applied stress would have to be reduced by about 0.25% NBL for every degree rise above 28°C to maintain the probability of failure at 1 in 10^6 .

7.14 Environmental effects on stress-rupture

When Kevlar is subjected to prolonged doses of ultraviolet (U.V) radiation (a component of sunlight) its tensile strength reduces significantly. At the beginning of the LLNL stress-rupture program the degrading effects of UV were unknown but it soon became clear that UV radiation, emanating from the laboratory fluorescent lighting, was having a marked effect on the stress-rupture data. UV degradation is now thought to be the cause of the downturn in the curve for stress versus the logarithm of lifetime for the LLNL yarns exposed to laboratory lighting. Figure 7.20 illustrates the considerable differences in stress-rupture behaviour for specimens subjected to, and protected from, UV radiation.

Under zero stress a degradation in strength of approximately 5% per year has been reported for the LLNL Kevlar 49/epoxy yarns subjected to laboratory fluorescent lighting (Chiao et al.1977a, Christensen and Glaser 1983). The degradation is even more severe when Kevlar is subjected to intense sunlight; a loss in tensile strength of about 16% per year has been reported for a 0.5 inch diameter Kevlar rope, despite the self-screening that would be expected from the bulkier Kevlar products (Table 2.4)

Glaser et al.(1984) recommend the use of the data from pressure vessel tests conducted in darkness, only for applications in which no hostile environmental effects are present. Since Parafil ropes are protected from UV radiation by an outer sheath then the use of the pressure-vessel data would seem justified. However there is now some evidence that another common environmental factor, humidity, might affect the stress-rupture behaviour of Kevlar.

Morgan et al.(1984) have observed hydrolytic degradation in Kevlar 49 fibres. The rate of hydrolysis was believed to be directly proportional to the relative humidity (R.H.) and to increase with temperature and applied stress. The effect of stress was taken into account by assuming that stress lowers the activation energy for hydrolysis. From experimental observations of hydrolytic degradation in stress-free Kevlar fibres, at various elevated temperatures and 100% R.H., Morgan et al.(1984) were able to estimate the degradation that would be expected for different service conditions. The

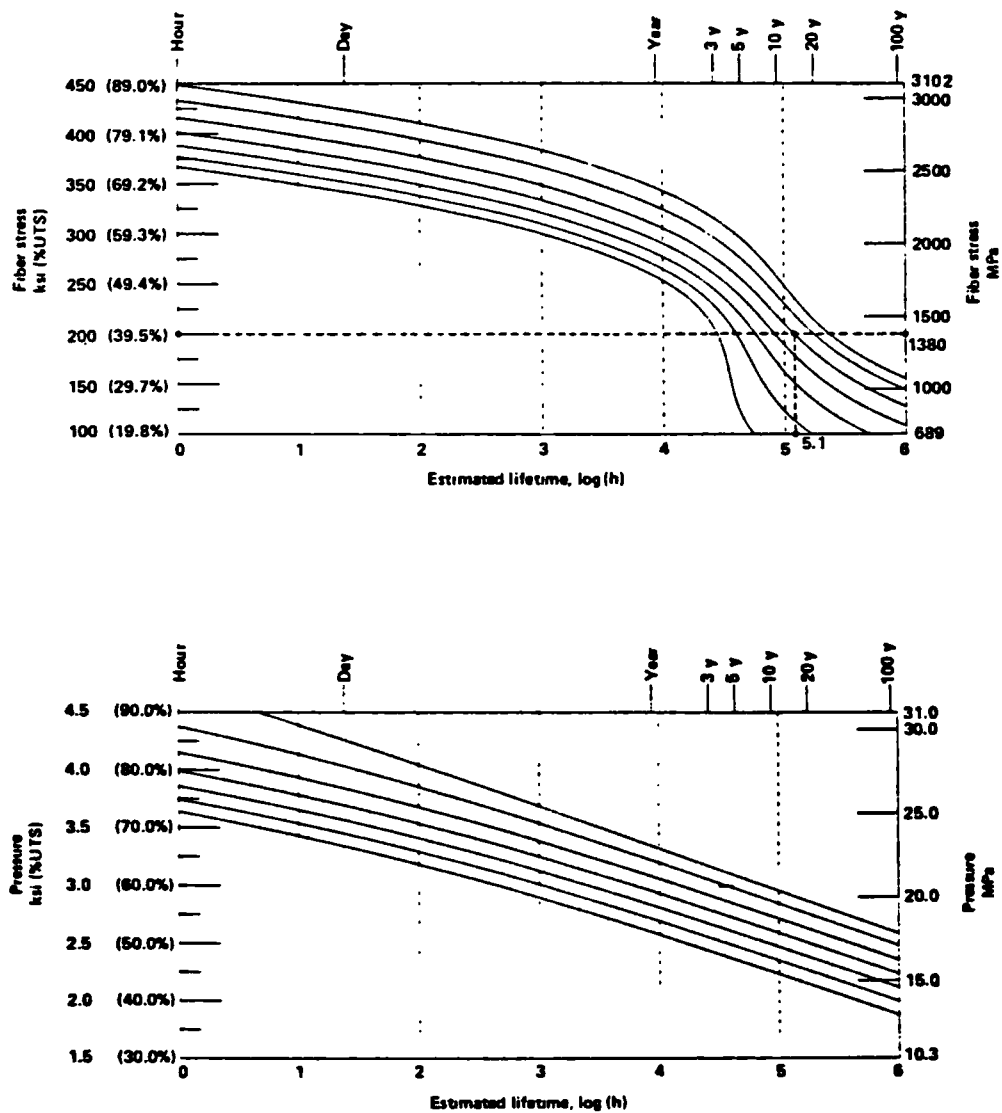


Figure 7.20 Difference in stress-rupture behaviour between Kevlar 49 composite yarns subjected to (above) and protected from (below) ultraviolet degradation. The failure probabilities from left to right are 10^{-6} , 10^{-5} , 10^{-4} , 10^{-3} , 10^{-2} , 10^{-1} and 0.50 (Glaser et al.1984).

estimates are shown in Table 7.8; they indicate that hydrolytic degradation, similar to UV radiation, could also cause a downturn in the stress-rupture curve for Kevlar.

Of particular concern in the results of Morgan et al.(1984) is the estimated strength reduction of 3.2% per year at 23°C, 100% R.H. and an imposed stress level of 75% of the ultimate (the percentage rises to 13.5% per year at 38°C). Morgan et al.(1984) favour the 'sudden-death' creep failure mechanism for Kevlar, which is characterised (when hostile environments are absent) by 100% strength-retention until just prior to failure. A strength degradation of 3.2% per year due to hydrolysis may be interpreted, therefore, as the reduction in tensile strength that would be observed if a Kevlar fibre were withdrawn from a creep test and tested to failure. The reduction in fibre strength would result in the fibre being exposed to a higher stress-percentage, subsequently resulting in lower failure times.

Exposure conditions	Imposed stress level on fibre as % of ultimate	Estimated strength degradation %/year
65°C; 100% RH	0	12
65°C; 100% RH	20	18
65°C; 5% RH	20	0.9
38°C; 100% RH	20	3.8
38°C; 100% RH	75	13.5
23°C; 100% RH	0	0.6
23°C; 100% RH	20	0.8
23°C; 100% RH	75	3.2
23°C; 15% RH	20	0.1

Table 7.8 Estimated rates of hydrolytic strength degradation in Kevlar 49 fibres for typical service environments based on tests conducted under stress-free conditions (Morgan et al.1984).

The predictions for the long-term behaviour of Parafil have been based on the LLNL pressure vessel tests which were conducted at a relative humidity of 25% to 50%. However, a service environment for a prestressing tendon of 90% RH is not uncommon in some parts of the world (e.g Singapore and the Persian Gulf). Hence, if the work of Morgan et al.(1984) is substantiated further then the potential effect of hydrolytic degradation on Parafil when used in humid environments, would have to be considered.

Howard and Parratt (1985) have conducted stress-rupture tests on bare Kevlar 49 yarns immersed in water at 65°C. The water appeared to cause a reduction in lifetimes at high stress-levels (Figure 7.21); nevertheless, the results remained within the wide scatter band of results from tests conducted in air. At low stress-levels, however, and contrary to what would be expected from the work of Morgan et al.(1984) no detectable difference in lifetimes between tests conducted in air and water were observed.

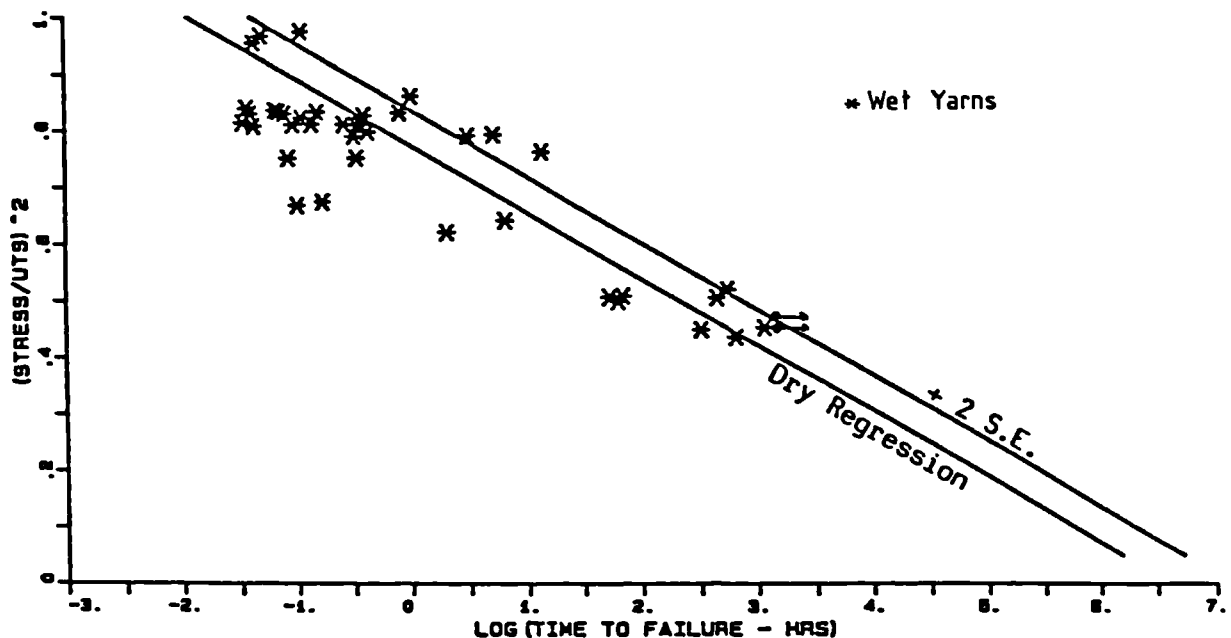


Figure 7.21 Comparison between the lifetimes of wet Kevlar 49 yarns at 65°C and the most probable lifetimes of dry yarns (Howard and Parratt 1985).

Howard and Parratt (1985) offered the explanation, for the apparent disagreement between their work and that of Morgan et al.(1984), that contaminants which could catalyse the ageing action in tests conducted at 100% RH are removed by immersion in water. Alternatively, using the observations made on wet creep behaviour as a basis, they argued that Kevlar could be more stable under stress than when stress-free. If this were true, then the lifetime reductions predicted by Morgan et al.(1984) based on stress-free tests, may never be realised in practical applications.

Clearly, research into how the environment affects the stress-rupture behaviour of Kevlar is incomplete. Morgan et al.(1984) have confirmed that temperature affects the hydrolytic degradation of stress-free Kevlar fibres at 100% RH; the results of these tests formed the basis for estimating lifetime reductions due to hydrolysis under likely operating conditions. Howard and Parratt (1985) on the other hand, have confirmed that water does not affect the stress-rupture behaviour of bare Kevlar 49 yarns at likely operating stresses. Hence, a study of the effect of varying RH on the lifetime of Kevlar 49 specimens subject to varying stresses (and temperatures) ought to be undertaken to resolve the problem.

CHAPTER 8: THE THEORETICAL AND EXPERIMENTAL STRENGTH DISTRIBUTION
OF 6 TONNE TYPE G PARAFIL ROPES

8.1 Introduction

Although Parafil ropes have been in production for some years it is believed that the distribution of their strength had not been determined prior to this investigation; the low operating stresses adopted in applications to date had probably meant that a knowledge of the approximate breaking loads of the ropes was sufficient for design purposes. However, because of structural and economic reasons, civil engineers tend to take full advantage of the strength of materials; for conventional materials (e.g. concrete and steel) this has rationally been achieved through a prior determination of strength distribution parameters relating to mean strength and variability.

The operating stresses for Parafil Type G ropes will be governed by criteria based on stress-rupture rather than initial strength; hence, the specification of parameters that describe the distribution of lifetimes at likely operating stresses would be most useful. The lifetime parameters, however, are difficult to obtain in practice but the tensile strength distribution parameters may be obtained fairly easily; moreover, the strength parameters are still important to engineers irrespective of stress-rupture. For instance, a knowledge of the strength parameters will enable engineers to make an instant assessment of Parafil's potential capabilities. Also (although difficult to prove) it is probable that the variability in tensile strength will be related to the variability in stress-rupture lifetimes (8.10.2).

If the distribution of tensile strength for individual Kevlar 49 yarns is known then the strength and variability of Parafil Type G ropes may be predicted using a theory which describes the statistical properties of bundles of fibres (or yarns). If the mean strength of Parafil as observed in experiments is found to be lower than the theoretical mean strength then there could be economic implications in terms of potential strength gains (depending on whether the manufacturing process could be made more consistent). However, if the experimental variability in rope strength is significantly larger than the

variability predicted theoretically then the implication is more serious; it could indicate that the assumption regarding the in-built conservatism of the LLNL stress-rupture model (based on Kevlar 49 yarns) when applied to Parafil (see 7.10) is simply invalid.

Another reason for determining the tensile strength distribution of Parafil is that it enables the effect of varying other engineering parameters to be assessed through an examination of the statistics. For instance, a theory for predicting the strength of bundles from the strength of individual yarns indicates that bundle strength decreases with increasing length; hence, a study of the effect of length on the strength of Type G Parafil ropes is important given that prestressing tendons can be several hundred metres long. Also, an investigation of the effect on strength of stressing a rope around a point of deflection is important since many tendons, for structural reasons, follow a curved profile.

In this Chapter, therefore, a statistical distribution is first fitted to experimental strength data for individual Kevlar 49 yarns. From this distribution the strength and variability of Type G Parafil ropes is predicted using a statistical theory for the strength of bundles. The theoretical effects of yarn length and yarn modulus variability on rope strength are also considered.

The design and development of a purpose-built rig for testing 6 tonne Type G Parafil ropes is discussed. Tests to determine the tensile strength distribution, the effect of length on strength, batch effects and the effect of stressing 6 tonne ropes around a deflector are then described. Finally, comparisons between experimental and theoretical behaviour are made and any discrepancies investigated.

8.2 A statistical distribution for the strength of Kevlar 49 yarns

The distribution is based on the results of 120 tensile strength tests performed by ICI Linear Composites Ltd. on bare Kevlar 49 yarns. The yarns tested were the same diameter as those incorporated in Type G Parafil ropes (i.e. nominally 2370 decitex). Five yarns were chosen from each of twenty-four different spools and it was assumed that this was statistically equivalent to selecting randomly one group of 120

yarns. Measurements of decitex (yarn size), breaking load, ultimate strain and toughness were taken for each test as part of ICI's normal quality control procedure. The gauge length of the yarn was kept constant at 0.5 metres.

The large number of test results was ideally suited to computer manipulation and so a datafile containing the results was set-up on a BBC microcomputer. From the test data the tensile strengths in engineering units (MPa) were calculated assuming a specific gravity of 1.44 for Kevlar 49. Figure 8.1 shows a histogram of the results.

A distribution commonly used to describe the strength of engineering materials is the normal or Gaussian distribution. The histogram suggests, however, that the distribution of tensile strength for Kevlar 49 yarns is non-Gaussian.

A better method of testing for compliance with the normal distribution is to draw the cumulative distribution on normal probability paper; the resultant plot should be a straight line. Hence, a computer program was written to arrange the results in ascending order and to determine the median rank for each result. The use of median ranks eliminates the subjectivity associated with choosing a class interval and permits cumulative distribution plots to be drawn rationally. The following approximate formula was used to determine the median ranks (Lipson and Sheth 1973):

$$\text{median rank} = (j - 0.3) / (n + 0.4) \quad 8.1$$

where: j is the failure order number
 n is the sample size ($n = 120$).

The resulting distribution, shown in Figure 8.2, is clearly non-linear, confirming that the distribution of tensile strength for these yarns is indeed non-Gaussian.

A versatile distribution, widely used to describe 'life' data for engineering components, but also popular in the literature for describing various data distributions for tensile fibres is the Weibull distribution. Coleman (1958) suggested reasons as to why the tensile strength of brittle fibres should obey the Weibull

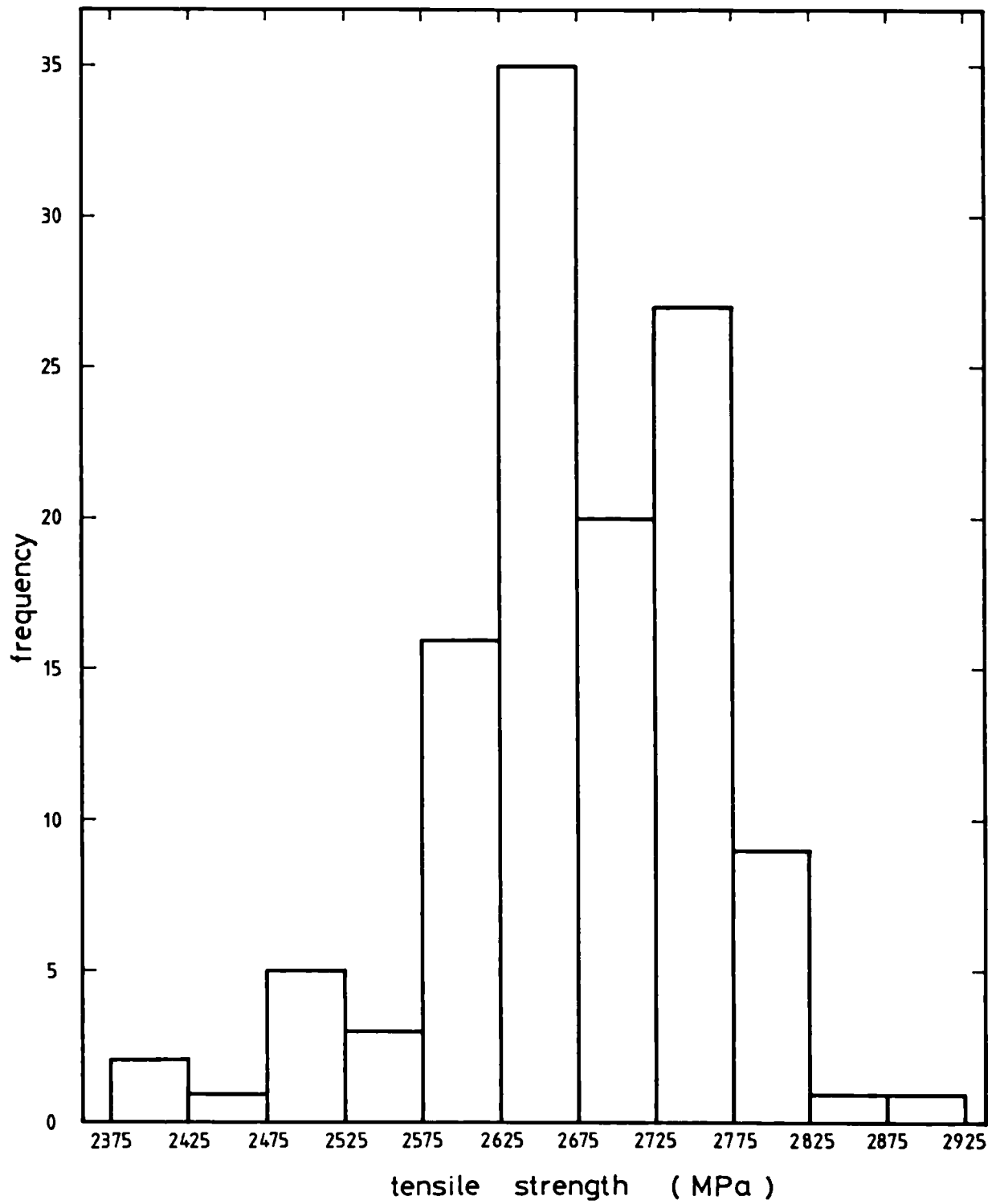


Figure 8.1 Histogram of Kevlar 49 yarn strength data.

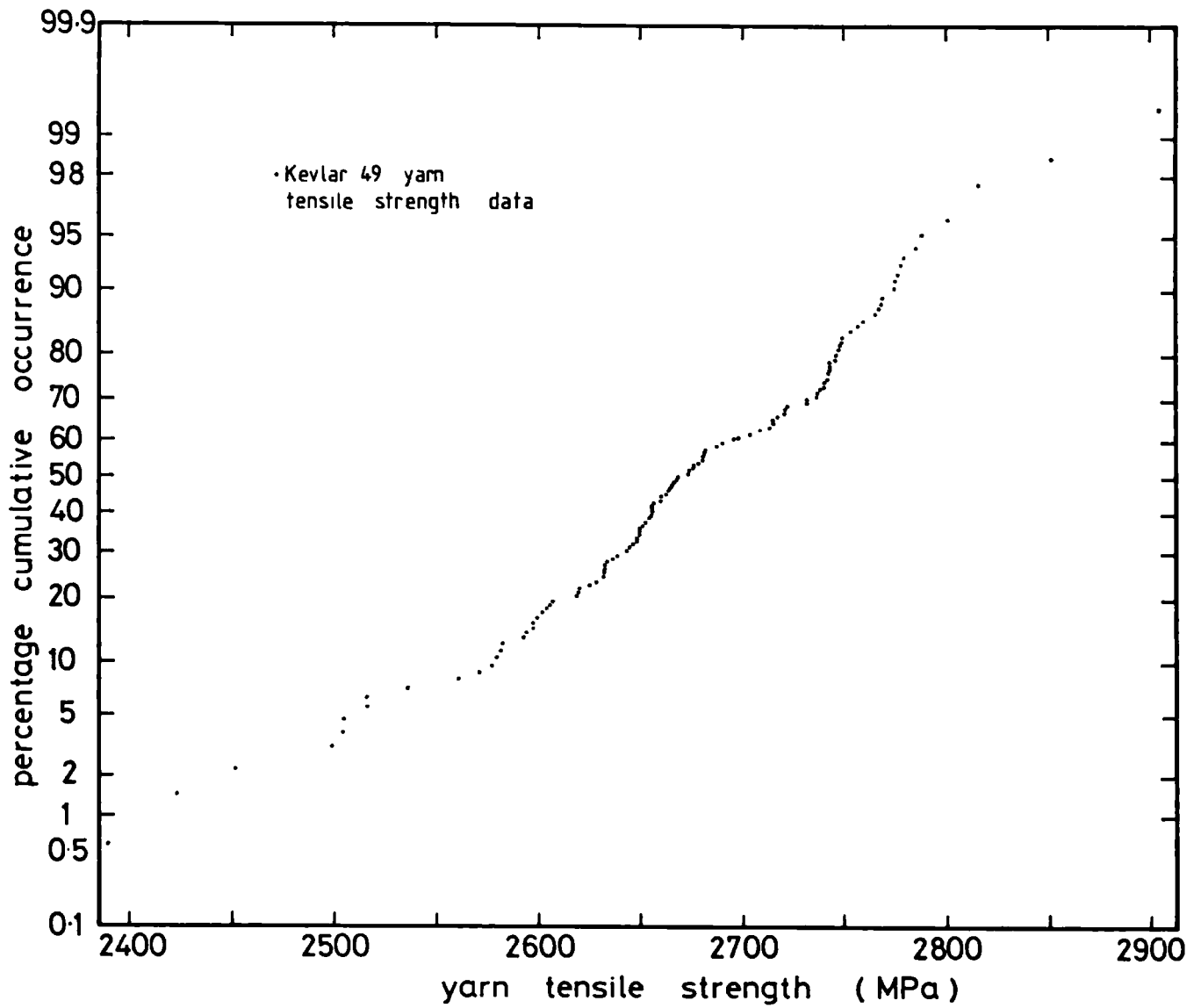


Figure 8.2 Cumulative distribution for Kevlar 49 yarn strength drawn on normal probability paper.

distribution. Subsequently, Wagner et al.(1984) confirmed Coleman's expectations in finding the distribution to be the most appropriate for describing the tensile strength of Kevlar 49 filaments.

The Weibull distribution is described in many texts (e.g. Lipson and Sheth 1973); it has a cumulative distribution function of the form:

$$F(x) = 1 - \exp \left[- \left(\frac{x - x_0}{\theta - x_0} \right)^b \right] \quad 8.2$$

where: x_0 is the expected minimum value of the property x and is known as the location parameter

b is the Weibull slope and is referred to as the shape parameter

θ is the characteristic value or the scale parameter.

The central tendency of the distribution is expressed by the scale parameter (θ) and the uniformity by the shape parameter (b) similar to the mean (μ) and the standard deviation (σ) of a normal distribution.

If the expected minimum of the population (x_0) is taken as zero then Equation 8.2 reduces to the two-parameter distribution given by:

$$F(x) = 1 - \exp [-(x/\theta)^b] \quad 8.3$$

The two-parameter Weibull distribution is the one most widely used to describe the distribution of tensile strength for textile fibres. If Equation 8.3 is expressed in terms of natural logarithms and rearranged then the following equation is obtained:

$$\ln [\ln \{1/(1 - F(x))\}] = b \ln x - b \ln \theta \quad 8.4$$

Equation 8.4 is of the form $Y = BX + C$ and so a plot of $\ln [\ln \{1/(1 - F(x))\}]$ against $\ln x$ should be a straight line if the given data follows a Weibull distribution. Figure 8.3 indicates that the plot for the Kevlar 49 strength data is sensibly linear and hence that the distribution of tensile strength for Kevlar 49 yarns may be described adequately by a two-parameter Weibull distribution.

The Weibull parameters, b and θ , can be estimated easily from Figure

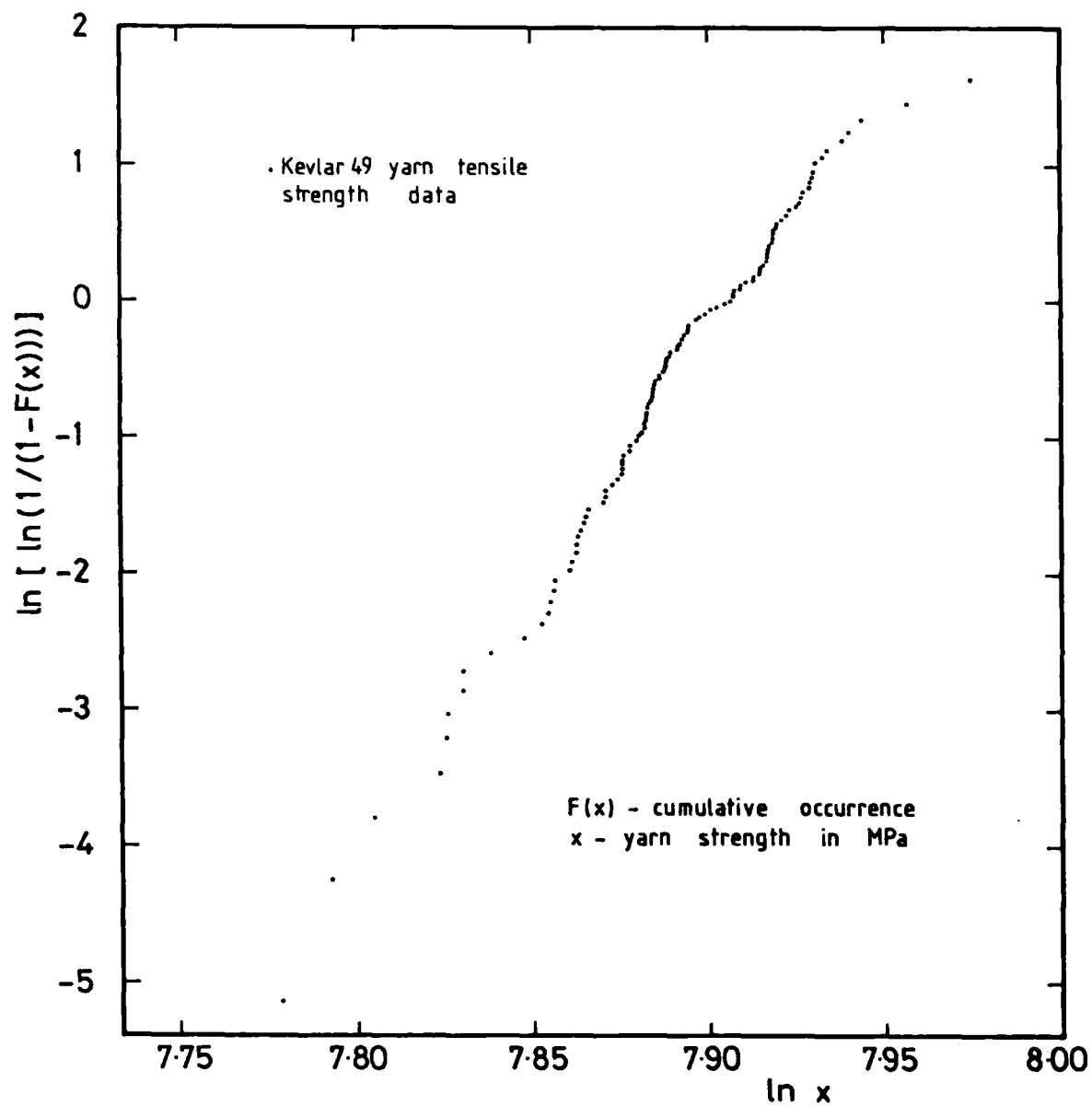


Figure 8.3 Cumulative distribution for Kevlar 49 yarn strength drawn on Weibull probability paper.

8.3 but a more formal and more accurate approach is to use the Method of Maximum Likelihoods. This method, in relation to the two-parameter Weibull distribution, is described by Cohen (1965) and involves an iteration which is tedious unless a computer is used. Using the latter method the Maximum Likelihood Estimates of the Weibull parameters for ICI's Kevlar 49 strength data were found to be:

Shape parameter (b) = 34.6

Scale parameter (θ) = 2714

with standard errors of 2.3 and 7.6 respectively (the standard error of an estimate is a measure of the uncertainty of that estimate due to sampling variation).

Hence, the cumulative distribution function for Kevlar 49 yarn strength may be expressed as:

$$F(x) = 1 - \exp \left[- (x/2714)^{34.6} \right] \quad 8.5$$

where $F(x)$ is the probability that yarns will exhibit a tensile strength less than x MPa.

8.3 The theoretical strength efficiency of Parafil ropes

8.3.1 Introduction

The strength of a rope is less than the average strength of its constituent fibres or yarns. Some of the reduction in strength is attributable to the structure of the rope as discussed in 3.1; for example, problems of abrasion can cause premature failure in ropes of twisted construction. However, even ropes of parallel-lay construction display strengths significantly less than would be expected on the basis of mean individual fibre strength. The reduction can be attributed to the variability in tensile strength displayed by the individual components. When a monotonically increasing load is applied to a bundle of fibres one of the fibres will eventually fail and so redistribute its load to the surviving fibres. This process of progressive failure continues until the remaining fibres have

insufficient strength to maintain the applied load, in which case, the bundle fails. The observed rope strength is always less than the average member strength owing to the early failure of those fibres which make-up the leading lower tail of the fibre strength distribution, so causing the surviving fibres to become overloaded.

8.3.2 Background to bundle theory and its assumptions

The prediction of the strength of bundles, given the distribution of strength for the individual members, has been the subject of considerable research. The first widely accepted statistical theory for the strength of bundles of fibres, however, was proposed by Daniels (1945). The basic result of Daniels' work is that theoretically the strength of a large bundle of fibres should be normally distributed with a mean and variance that is dependent on the fibre strength distribution. Coleman (1958) was the first to apply the theory developed by Daniels (1945) to fibres whose strength followed the Weibull distribution.

The essential feature of the work conducted by Daniels and Coleman is that of equal loadsharing, i.e. all surviving fibres under a monotonically increasing load share the load equally. The assumption of equal loadsharing remains popular in the literature and seems particularly appropriate for Parafil ropes since they consist of parallel elements between which very little interaction would be expected. Less-developed theories for the strength of bundles in which there is localised load-sharing among fibres also exist (e.g. Pitt and Phoenix 1981) but these are intended to be applied to bundles in which there is a strong interaction between neighbouring fibres due to friction or a binding matrix.

In applying the results of Coleman (1958) to Parafil ropes the assumption that a yarn plays the role of a single fibre and several yarns grouped together form a bundle has to be made. This assumption is not unprecedented (e.g. Phoenix 1976); besides, a primary concern is that the strengths of the individual components comprising the bundle follow a Weibull distribution which has been shown to be the case (8.2). Further assumptions that need to be made when applying Coleman's results to so-called 'classical' bundles are:

- (1) The fibre strength is independent of the rate of loading
- (2) The stiffness of the fibres is constant
- (3) The bundle contains an infinite number of fibres (8.3.3).

The assumption that fibre strength is time-independent appears to be fundamental to most bundle theories presumably because the introduction of a time parameter would cause a major restriction in their development. Gourdin (1980) showed that the strength of Kevlar 49 was insensitive to the rate of loading (although the range investigated was quite small); nevertheless, despite the small range considered, the assumption of time-independent strength for Kevlar 49 yarns at typical testing rates seems reasonable.

Measurements of the strain at failure were taken during the ICI yarn tests (reported in 8.2) and so the stiffness of each yarn could be estimated (a linear stress-strain curve was assumed). The scatter in the stiffness results was not large and so for the time being a constant fibre stiffness is assumed. Later, an estimation of the effect of yarn stiffness variability on bundle strength is made (8.3.6).

8.3.3 Strength efficiency evaluation

Coleman (1958) found that the ratio of the tensile strength of a bundle containing an infinite number of elements to the average tensile strength of its elements was given by:

$$\epsilon_{\infty} = \frac{b^{-1/b} \exp(-b^{-1/b})}{\Gamma(1 + 1/b)} \quad 8.6$$

where: ϵ_{∞} is known as the tensile strength efficiency for an infinite bundle

b is the Weibull shape parameter for the individual elements

Γ is the standard gamma function.

(McCartney (1982) derived a similar expression from a consideration of the distribution of defects within a fibre; the denominator of

Equation 8.6 was absent from McCartney's equation but for typical values of the shape parameter, b , the gamma function approaches unity).

Figure 8.4 illustrates the dependence of the tensile strength efficiency of a bundle containing an infinite number of fibres on the coefficient of strength variation of the component fibres. As expected, when there is zero variability in the strength of the individual members (i.e. they all fail at the same stress) the strength of the bundle is equal to that of its components. As the strength variability of the fibres increases, however, so the tensile strength efficiency drops rapidly.

Results from the statistical theory described here will later be compared with the experimental behaviour of 6 tonne Type G Parafil ropes. Six tonne ropes contain less than 200 individual yarns and so the effect of the number of components (n) on the strength efficiency ought to be considered before Coleman's results for an infinite bundle are accepted. Phoenix (1976) considered the size problem and concluded that the asymptotic result (i.e. Equation 8.6) provided a good strength approximation for bundles containing more than 50 elements; hence, the asymptotic result is appropriate for the size of Parafil rope under consideration. Figure 8.5 illustrates the effect of bundle size and fibre variability on the bundle strength efficiency.

The maximum likelihood estimate of the Weibull shape parameter for the Kevlar 49 yarn strength distribution was found to be 34.6 (8.2). When this value is substituted into Equation 8.6 it is found that the bundle strength efficiency is 0.89 i.e. for a given length, a Parafil rope containing a large number of yarns ought to have about 89% of the average strength of all the individual yarns. For ropes containing fewer yarns (<50 say) the strength of Parafil ropes (in percentage terms) should be greater than that predicted by the asymptotic result in accordance with Figure 8.5.

A major conclusion to be drawn from the bundle theory discussed so far, and one that should be given the utmost consideration in any rope production facility, is that a small increase in the variability of fibre (or yarn) strength causes a large decrease in the mean strength of the rope (Figure 8.5).

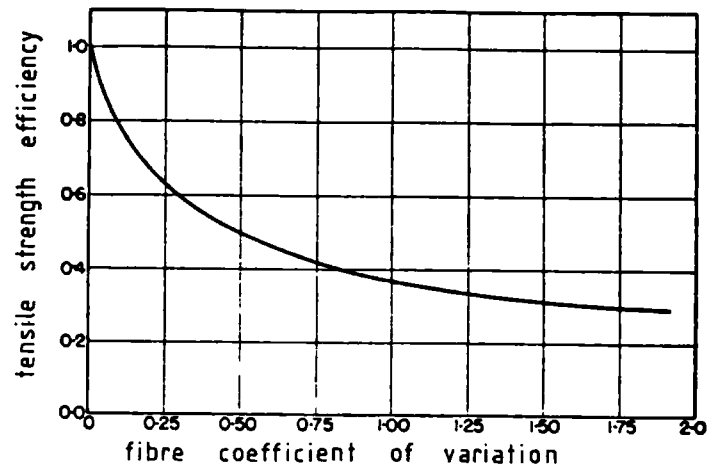


Figure 8.4 Theoretical effect of fibre strength variability on the strength of a bundle containing an infinite number of fibres (Coleman 1958).

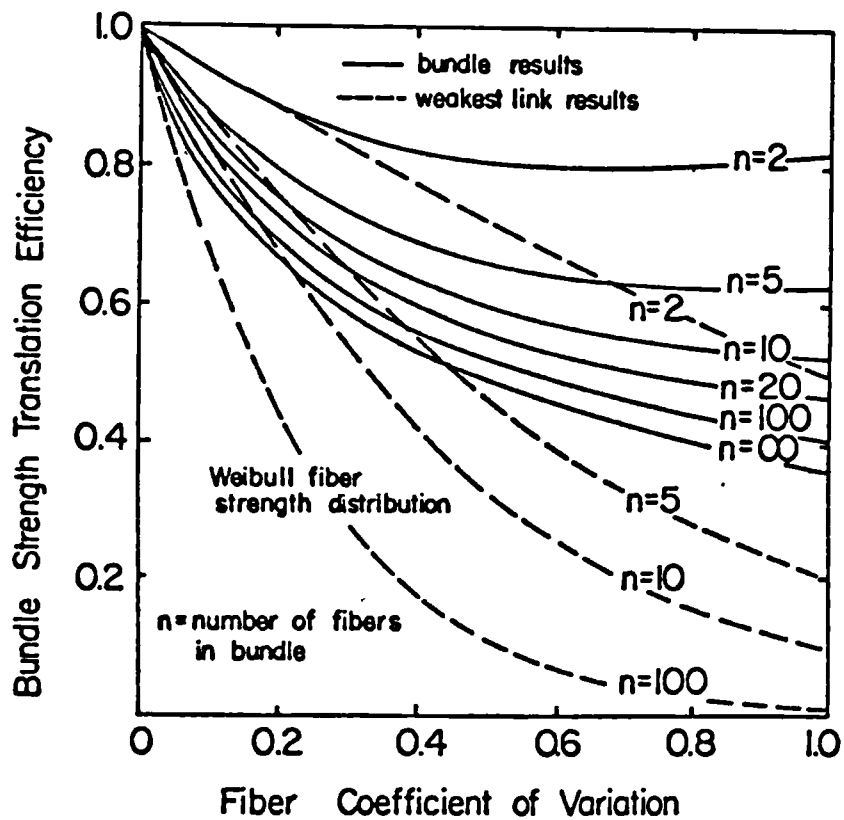


Figure 8.5 The effect of bundle size and fibre coefficient of variation on bundle strength (Phoenix 1976).

8.3.4 Strength variability evaluation

Using the results of Coleman (1958) it can easily be shown (e.g. by using the expressions found in Pitt and Phoenix 1981) that the coefficient of variation (c.v.) of an infinite bundle of classical fibres is given by:

$$\text{c.v.} = \left\{ \left(e^{1/b} - 1 \right) / n \right\}^{0.5} \quad 8.7$$

where: b is the fibre shape parameter
 n is the number of fibres.

If the number of yarns found in a 6 tonne Parafil rope is substituted into Equation 8.7 then the theoretical coefficient of variation for the rope is found to be 0.0126. The experimental coefficient of variation of Kevlar 49 yarns as observed in the tests discussed earlier (8.2) was 0.0320 and is therefore considerably greater than that for a large bundle.

The difference in the coefficients of variation is symptomatic of the second major, and perhaps rather surprising conclusion that can be drawn from bundle theory i.e. the variability in fibre (or yarn) strength has little effect on the variability of bundle (rope) strength (as indicated by varying the shape parameter, b , in Equation 8.7). The fibre strength variability does, however, have a major effect on the mean strength of the bundle as described earlier (8.3.3).

8.3.5 The effect of length

In presenting the reasons why a Weibull distribution was suitable for describing the strength of classical fibres Coleman (1958) considered each fibre to be a set of ℓ non-interacting unit lengths. The Weibull distribution after Coleman (1958) may, therefore, be written:

$$F(x) = 1 - \exp \left[- \frac{\ell}{\ell_0} \left(\frac{x}{\theta} \right)^b \right] \quad 8.8$$

where: ℓ is the fibre length

ℓ_0 is a constant taken as unity but with units of length.

To determine the theoretical strength of a bundle having the same length as that of its component fibres the length term need not be taken into consideration; however this is not the case when the length varies. Theory predicts (e.g. see Wagner et al.1984) that the strengths of fibres (or yarns) drawn from the same sample but with length different from that of the parent distribution also obey a Weibull distribution with the same shape parameter, b , as the parent distribution but with a new scale parameter, θ' , given by:

$$\theta' = \theta \left(\frac{\ell'}{\ell} \right)^{-1/b} \quad 8.9$$

where: θ' is the scale parameter of the new distribution (whose fibres have length ℓ')

θ is the scale parameter of the parent distribution (whose fibres have length ℓ)

b is the shape parameter of the parent distribution.

Hence, the theoretical mean strength of a bundle of length, ℓ' , may be found by scaling the mean strength of a bundle of length ℓ by the factor $\left(\frac{\ell'}{\ell} \right)^{-1/b}$. The variance of the bundle of length ℓ' , likewise,

should be scaled by the factor $\left(\frac{\ell'}{\ell} \right)^{-2/b}$, but the coefficient of

variation (defined as the square root of the variance divided by the mean strength) of the new bundle naturally remains unchanged; hence, the variability in bundle strength as defined by the coefficient of variation is independent of its length.

The ICI Kevlar 49 yarn tests (described in 8.2) were carried out on specimens of 0.5 metres length and the corresponding theoretical strength efficiency factor for a Parafil rope of the same length (with more than 50 yarns say) was found to be 0.89 (8.3.3). To determine the tensile strength efficiency for Parafil ropes of length greater than 0.5 metres, therefore, this factor needs to be multiplied by a length reduction factor; the effect of length on the reduction factor is shown in Table 8.1.

Rope length, l' (metres)	length reduction factor, $l_r = \left[\frac{l'}{0.5} \right]^{-1/34.6}$	Overall tensile strength efficiency (= $0.89 l_r$)
2.9*	0.95	0.85
10.9*	0.91	0.82
50	0.88	0.78
1000	0.80	0.72
5000	0.77	0.68

* these lengths correspond to those of test specimens

Table 8.1 Theoretical effect of rope length on strength.

8.3.6 The effect of stiffness variability

One of the main assumptions made in bundle theory is that the stiffness of the individual elements is constant. In practice, however, some variation in the stiffness of the elements comprising a bundle is inevitable and this could affect the bundle strength efficiency.

During the tests on Kevlar 49 yarns the strain at failure (recorded as a percentage strain) was measured to two significant figures; hence, only an approximate determination of yarn modulus was possible. Despite the inaccuracy, it was possible, using an analysis of variance, to establish that the variation in modulus between different spools of yarn was much greater than that within individual spools and hence, that the effect of modulus variability warranted further investigation.

To determine the effect, the yarns were first classified according to their observed failure strains and the average modulus of each group of yarns was calculated from the observed failure stresses. Table 8.2 indicates the classification of yarns based on failure strain. From a

Failure strain (%)	Number of yarns	Average modulus (GPa)
2.0	1	119.5
2.1	3	117.6
2.2	11	116.6
2.3	67	116.3
2.4	35	113.6
2.5	3	110.1

Table 8.2 Classification of Kevlar 49 yarn test data according to failure strain.

consideration of strains intermediate to those shown in Table 8.2 (e.g. 2.05%, 2.15% etc) the number of surviving yarns, and hence the average stress sustained by each surviving yarn is determined. Finally, an assessment of the load carried by the bundle at each level of strain is made. The sequence of operations is clarified in Table 8.3.

The load carried by the bundle is maximised at a strain of 2.15%; if the maximum load is divided by the bundle load calculated from the mean yarn strength then the approximate theoretical strength efficiency based on strains and accounting for modulus variability is obtained. This calculation is illustrated in Table 8.3 where the tensile strength efficiency based on strain measurements is shown to be 0.90 (c.f. 0.89 based on yarn strength). It may be concluded, therefore, that the modulus variability observed within the Kevlar 49 yarns ought to have an insignificant effect on rope strength.

Applied strain, ϵ (%)	Number of surviving yarns, N	Average modulus of surviving yarns, E (GPa)	Stress borne by surviving yarns, $\sigma = 10 \cdot E \cdot \epsilon$ (MPa)	'Load' carried by bundle, $L = N \sigma$ ($\times 10^{-3}$)
1.95	120	115.44	2251	270
2.05	119	115.41	2366	282
2.15	116	115.35	2480	288
2.25	105	115.22	2592	272
2.35	38	113.32	2663	101
2.45	3	110.10	2697	8
2.55	0	0	0	0
mean strength of individual Kevlar 49 yarns = 2673 MPa hence, tensile strength efficiency $= \frac{288 \times 10^3}{120 \times 2673} = 0.90$				

Table 8.3 The determination of strength efficiency based on strains and moduli.

8.4 Experimental design

The main aims of the experimental study were:

- (1) to determine the distribution of tensile strength for a Parafil Type G rope.
- (2) to investigate the effects on strength of:
 - (a) rope length
 - (b) stressing ropes around a deflector
 - (c) testing ropes from different batches
- (3) to compare the experimental results with those predicted from bundle theory.

Six tonne ropes were chosen for the study since:

- (1) they are relatively inexpensive
- (2) they are easy to handle and to terminate
- (3) they enabled the laboratory floor to be used as a reaction frame without the need for large floor plates
- (4) in terms of bundle theory they contain a sufficiently large number of yarns for strength comparisons to be made with the theoretical results for an infinite bundle.

To determine the parameters for any distribution a large random sample should be chosen. For instance, in the case of a normal distribution Lipson and Sheth (1973) recommend that at least thirty specimens should be tested so that the standard deviation of the population (σ) may be approximated by the standard deviation of the sample (s); once the standard deviation of the population is known the mean of the population can be determined with confidence.

Many authors have shown that if the strengths of fibres (or yarns) follow a Weibull distribution then the strength of a large bundle of those fibres should follow a normal or Gaussian distribution (Daniels 1945, Coleman 1958, Phoenix 1976). A six tonne Type G rope contains in excess of 150 yarns and so may be classed as a 'large' bundle (8.3.3); hence, the distribution for the strength of these ropes ought to be Gaussian. At the experimental design stage, therefore, it was decided to test a minimum of 30 specimens for the purposes of determining the strength distribution; in practice, this number proved sufficient since the scatter observed was small (8.9.1). Ideally, the thirty specimens should have been randomly chosen but for practical

reasons ropes from a single batch were tested. It was considered initially that since these ropes contained yarns from many different spools, batch effects would be negligible. However, to test this assumption ten ropes from a second batch were also tested.

To test whether there were any significant reductions in rope strength due to rope length or stressing ropes around a deflector it was necessary to perform only a limited number of further tests; six tests of each type proved sufficient. Table 8.4 gives a summary of the tests undertaken.

Batch	Series	No. of tests performed	Rope length (m)	Subject of investigation
1	1	30	2.9	Distribution
1	2	6	10.9	Length effects
1	3	6	2.9	Deflector effects
2	1	10	2.9	Batch effects

Table 8.4 Tests conducted on 6 tonne Type G Parafil ropes.

8.5 Six tonne test rig

8.5.1 Introduction

The main criterion governing the design of the test rig was versatility. In particular, the rig had to provide a facility for:

- (1) testing ropes with a nominal strength of 6 tonnes through to failure
- (2) handling ropes of varying length
- (3) dealing with deflected ropes
- (4) enabling the characteristics of bond between Parafil and concrete to be determined as part of a future research programme.

8.5.2 End-blocks

The basic test rig consisted of two steel end-blocks as shown in Figures 8.6 and 8.7. Sufficient frictional restraint against horizontal movement was developed by stressing each end-block to the laboratory floor with a force of 40 tonnes (i.e. 20 tonnes was applied to each bolt).

The floor bolts throughout the laboratory are spaced at three-feet centres; hence, ropes of varying length could be tested simply by repositioning one or both of the end-blocks. For the deflected rope tests the box-sections of the blocks could be rotated on bearings so that they were concentric with the rope, thus avoiding kinks. Rotating bearings manufactured by the Nippon Pillow Block Manufacturing Co Ltd. (Model UCFL 2I2E) were found suitable for this purpose.

8.5.3 Stressing procedure

Stressing was performed using a single actuator, housed within the box-section of one of the rotating end-blocks as shown in Figure 8.8. The actuator, manufactured by Enerpac (Model RRH-3010) had a stroke of 257 mm and a capacity (in the tension mode) of 18 tonnes; it was powered by an electrically operated 5000 p.s.i. Amsler hydraulic cabinet. A pull-rod of 33 mm diameter located in the centre-hole of the actuator and secured with a conternut (Figure 8.7) completed the stressing arrangement.

The ropes were stressed from one end in these tests but the capability for stressing ropes simultaneously from both ends was built-in to the rig for future tests by having two identical end-blocks.

8.5.4 Universal joint and rope fittings

Standard terminals consisting of an aluminium alloy terminal body and a fork fitting were employed (Figure 3.16). The fork fitting permitted rotation in one plane when connected directly to the pull-rod; however, with the incorporation of a similar additional fitting a simple universal joint was formed. Figure 8.9 illustrates the

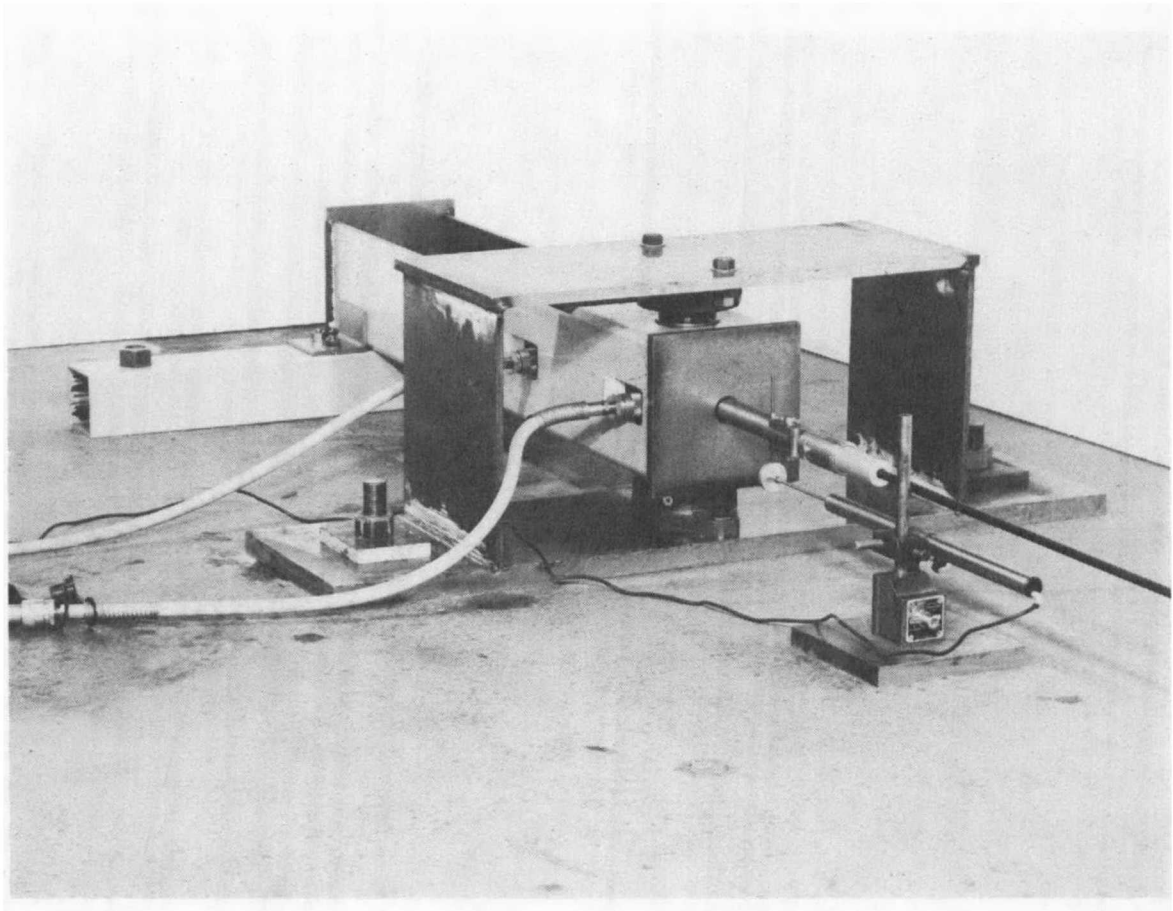


Figure 8.6 End-block of 6 tonne test rig.

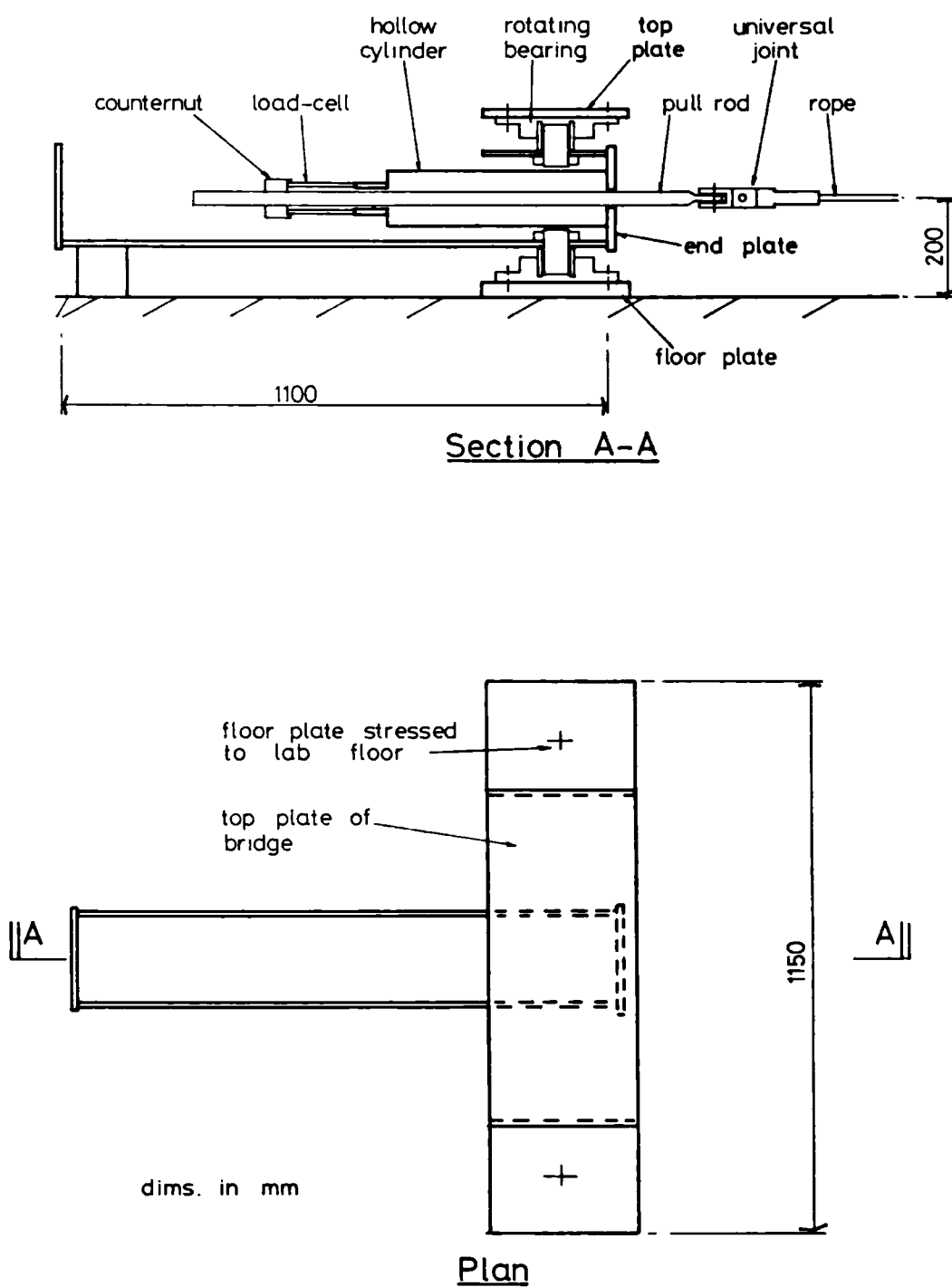


Figure 8.7 Details of end-block and stressing arrangement for 6 tonne test rig.

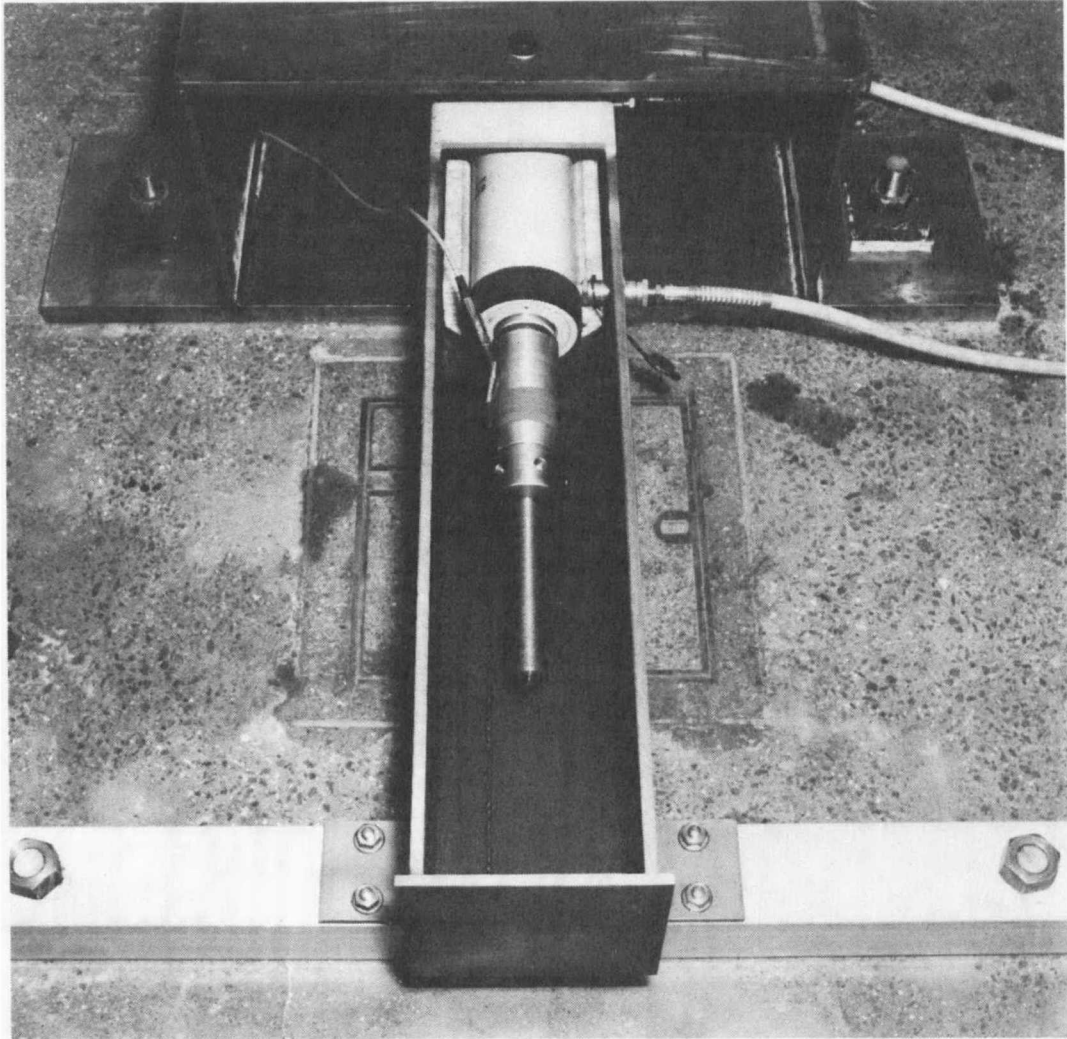


Figure 8.8 View of stressing jack housed within box-section of end-block.

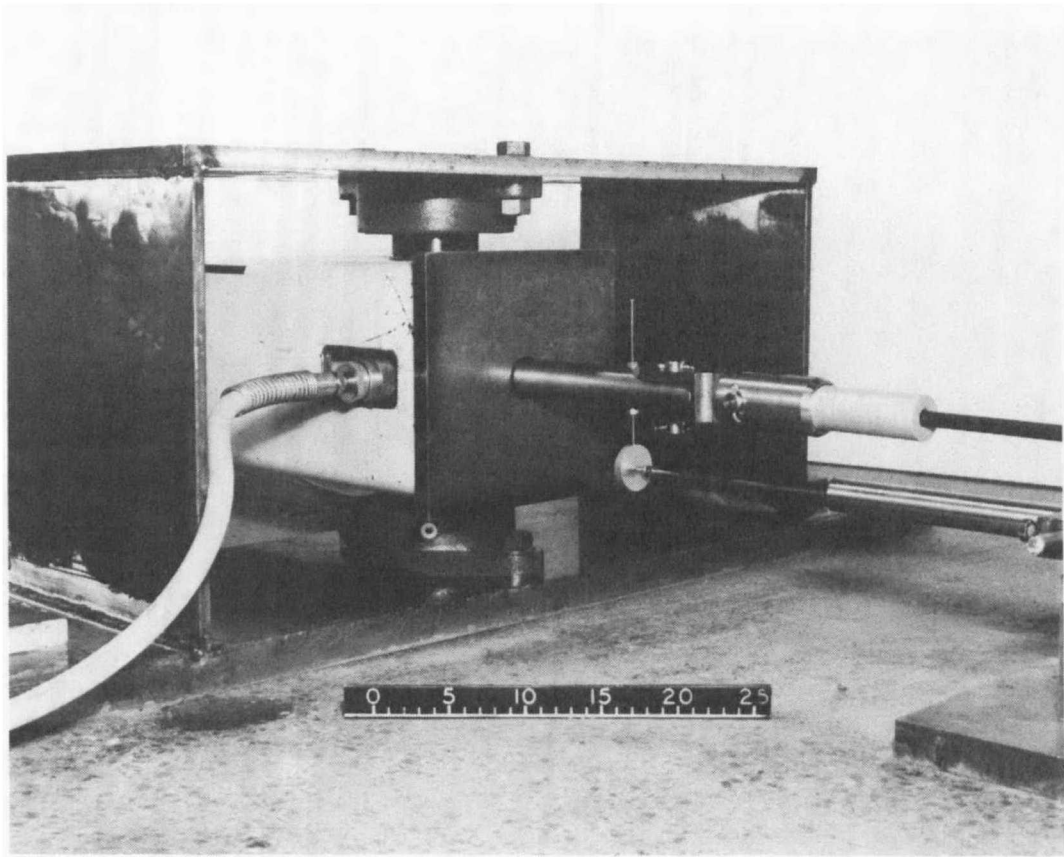


Figure 8.9 Close-up view of end-block showing universal joint and rotating bearings.

universal joint (and rotating bearings) at the stressing-end of the rig. The arrangement at the dead-end was very similar except that in this case a short length of pull-rod was clamped in position with locknuts either side of the box-section end-plate.

8.5.5 Deflector

Steel tendons exhibit lower strengths when stressed to failure around a point of deflection and for this reason limits on the radius of the deflector and the angle of deflection are specified in British Codes of Practice (e.g. CP110: Part 1: 1972). Figure 8.10 illustrates the deflector used in the deflected rope tests on Parafil. The radius of curvature of the deflector was equal to 50 times the diameter of the rope (550 mm) and the deflection angle adopted was 15° . The test conditions employed, therefore, were the most onerous permitted for a steel tendon.

8.6 Measurements and instrumentation

All test measurements were recorded using the Hewlett-Packard data acquisition system described earlier (4.4.2). Output voltages were recorded from:

- (1) a loadcell measuring the force in the rope
- (2) a linear variable differential transformer (LVDT) measuring total displacement of the rope plus bed-down within the end fittings.

The loadcell, made from aluminium, was located on the pull-rod between the ram of the actuator and the counter nut (Figure 8.8). Machined end-caps ensured that the loadcell was concentric with the actuator.

The displacement measured by the LVDT (Figure 8.6, 8.9) included the full extension of the rope plus the bed-down of the rope within the end fittings. It was not possible to measure the bed-down within the end-fittings continuously during these tests since standard terminals were used. However, the total movement of the spike was measured after pretensioning and after each strength test using a micrometer depth gauge.

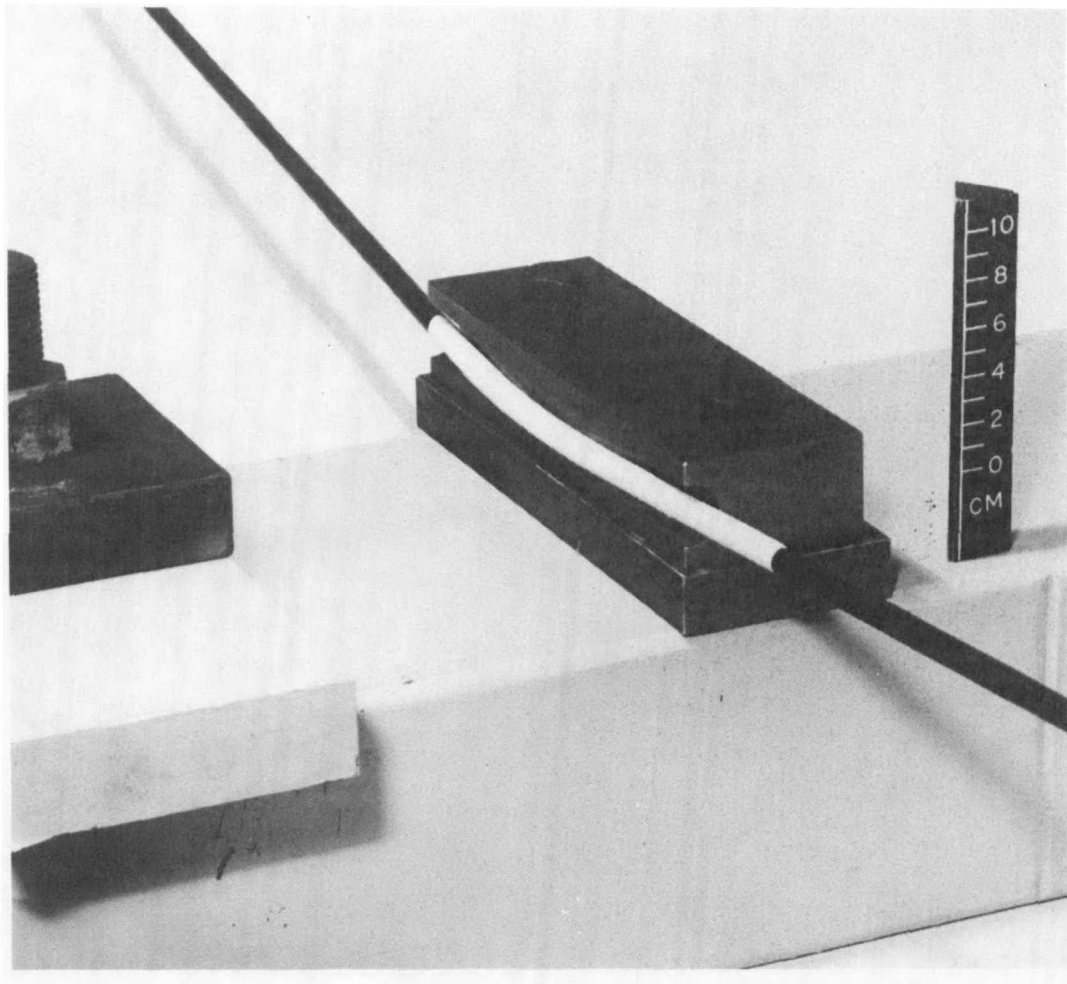


Figure 8.10 Steel deflector used in deflected rope tests.

Both instruments were calibrated in a manner similar to that described previously (4.4.3, 4.4.4). The accuracy of the loadcell was found to be ± 0.02 kN while that of the LVDT was ± 0.025 mm. The two output voltages were fed to an X-Y plotter as before (4.4.7).

8.7 Test procedure

8.7.1 Pretensioning

All ropes were pretensioned to 60% of the nominal breaking load (i.e. 3.6 tonnes) in the manufacturer's prescribed manner (4.5.2). The rate of strain employed was the same as that used in the tests on 60 tonne ropes i.e. 0.018% /second. The position of the spike in both fittings was determined using a micrometer depth gauge both before and after pretensioning.

In the deflected rope tests pretensioning was performed with the ropes located around the deflector; it is likely that in practice ropes would be pretensioned using the actual concrete member as the reaction frame (Chapter 9) and so this procedure simulated likely site conditions. PTFE tape was wrapped around the outer sheath of the rope to minimise friction in these tests; measurements of the extension of the outer sheath relative to the deflector indicated that the PTFE tape performed successfully.

8.7.2 Testing to failure

One hour after pretensioning the ropes were tested to failure. Figures 8.11 and 8.12 show a tension test on a straight rope and a deflected rope test in progress respectively. After failure the positions of the spikes within the terminals were noted.

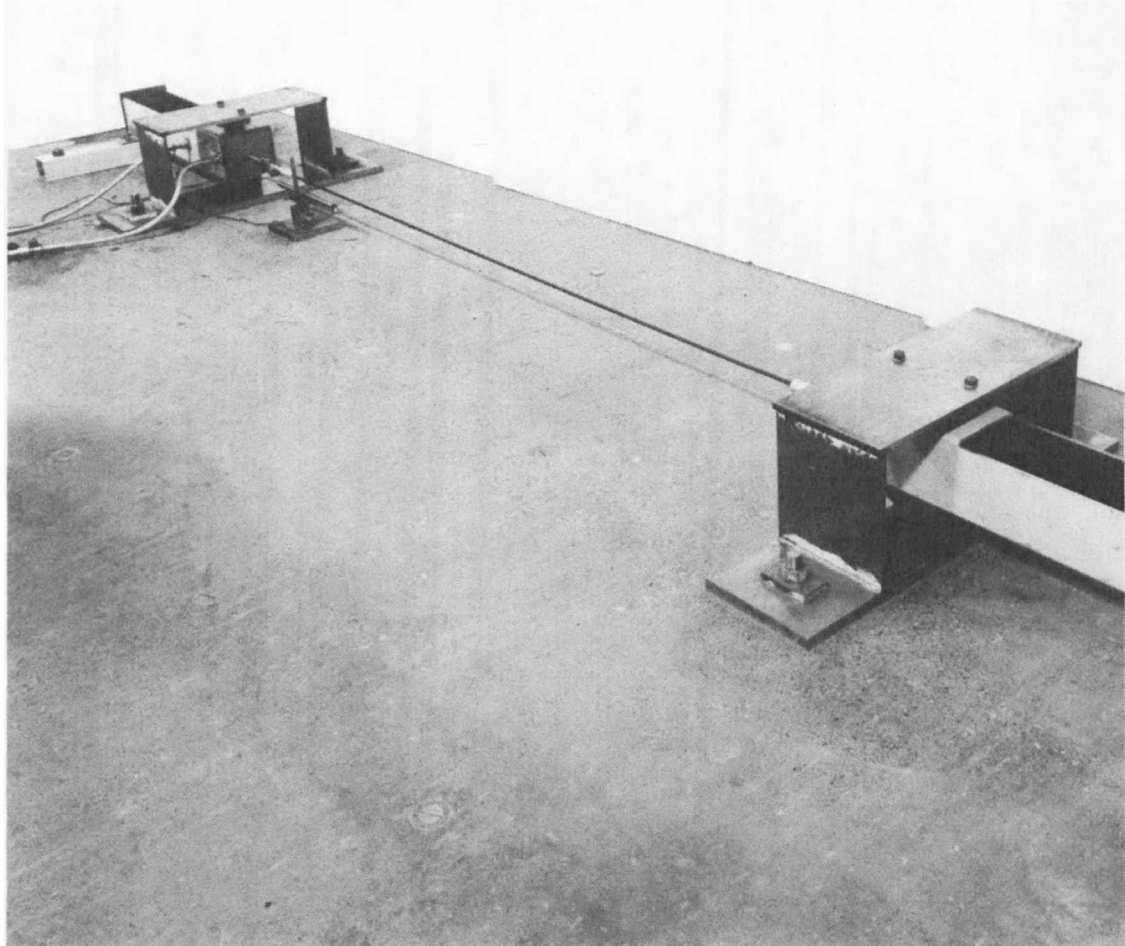


Figure 8.11 Tension test on a straight rope.

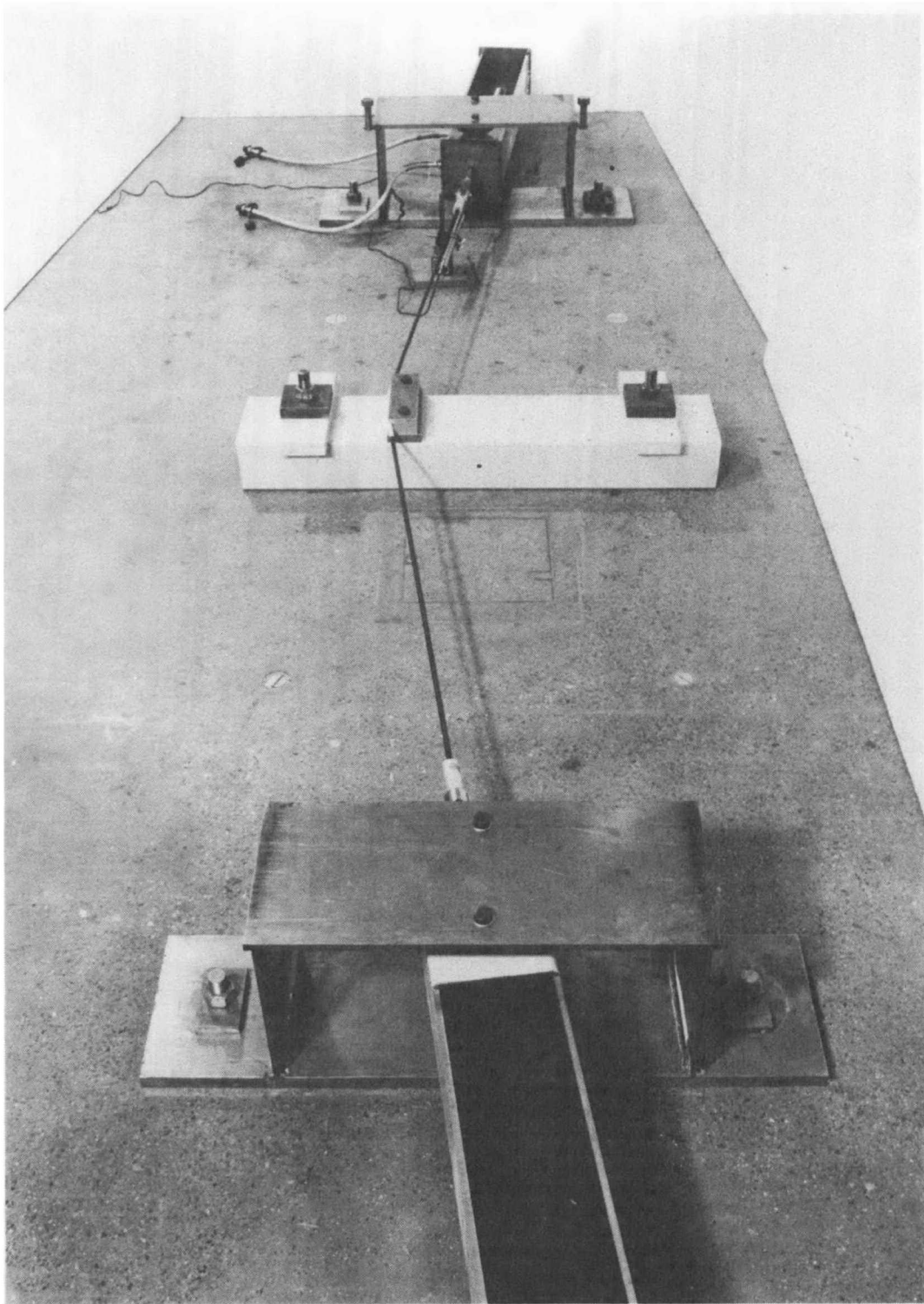


Figure 8.12 Tension test on a deflected rope.

8.8 Test results and discussion

8.8.1 Pretensioning

Although tensile strength was the rope property of prime concern in this investigation some typical results acquired during the usual pretensioning process will be reported for completeness.

Figure 8.13 indicates a typical X - Y plot obtained during pretensioning. In character with the behaviour of 60 tonne ropes (Chapter 4) the gradient of the load-displacement curve increased progressively up to the pretensioning limit. A permanent offset, $\Delta p'$, was observed on the displacement axis upon load removal; all ropes were pulled handtight prior to pretensioning and it was observed that this largely eliminated any initial rope slack; hence, this offset can be attributed to the bed-down of the rope within the fittings and the disorientation effect (4.5.2). The remainder of the observed total displacement, Δm , is due to the extension of the rope alone. Table 8.5 indicates typical pretensioning results; strains are quoted on the basis of the approximate rope length between spike tips.

8.8.2 Ultimate strength tests

The observed ultimate loads for each series of tests are indicated in Tables 8.6 to 8.9. Some additional results of secondary importance to this investigation are given in Table 8.10.

Figure 8.14 shows a typical X - Y plot for a rope that has been loaded to failure. The vertical axis of the plot represents the load in the rope while the horizontal axis represents the extension of the rope plus the small amount of bed-down that occurred within the fittings (see Table 8.10).

The mode of failure of the 6 tonne ropes was less brittle than observed in the 60 tonne ropes (4.5.3). In fact, the onset of failure was quite discernible and it would have been possible to unload a rope from its ultimate load while still leaving it intact. Consequently, the ultimate strain in the 6 tonne rope tests was less well-defined as shown in Figure 8.14. This observation is consistent with the bundle

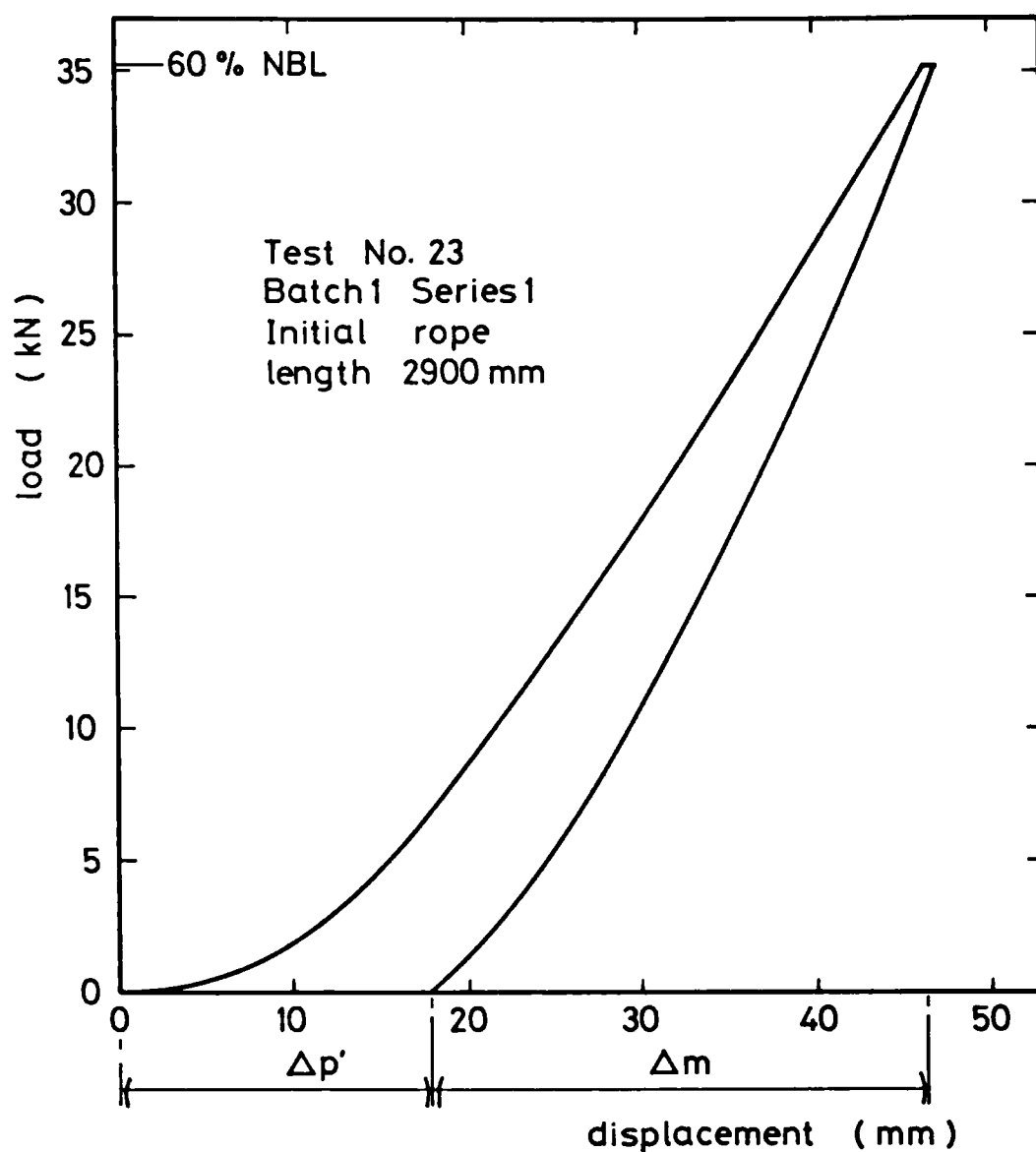


Figure 8.13 Typical X-Y plot obtained during pretensioning.

Bed-down in a single fitting (mm)	5.2
Disorientation strain (%)	0.28
Rope strain ($\Delta m/L$) at 60% NBL (%)	1.06

Table 8.5 Typical pretensioning data for 6 tonne Type G Parafil ropes.

Test number	Ultimate load (kN)	Test number	Ultimate load (kN)
1	61.77	16	61.42
2	60.25	17	61.80
3	61.88	18	62.38
4	62.19	19	60.64
5	61.60	20	61.11
6	62.81	21	62.10
7	60.64	22	62.44
8	60.95	23	61.81
9	63.74	24	62.30
10	63.72	25	60.86
11	62.16	26	61.42
12	61.91	27	61.83
13	62.21	28	62.28
14	62.21	29	60.72
15	61.91	30	61.30

Notes: Designation: Batch 1 Series 1
 Objective: to determine strength distribution
 Initial rope length: 2900 mm
 Strain rate: 0.018%/sec

Table 8.6 Ultimate load results for Batch Series 1.

Test number	Ultimate load (kN)	Test number	Ultimate load (kN)
31	60.84	34	61.54
32	58.74	35	61.22
33	61.84	36	60.95

Notes: Designation: Batch 1 Series 2
 Objective: to investigate length effects
 Initial rope length: 10900 mm
 Strain rate: 0.018%/sec

Table 8.7 Ultimate load results for Batch 1 Series 2

Test number	Ultimate load (kN)	Test number	Ultimate load (kN)
37	61.05	40	62.60
38	61.40	41	60.64
39	62.21	42	61.71

Notes: Designation: Batch 1 Series 3
 Objective: to investigate effects of stretching around a deflector
 Initial rope length: 2900 mm
 Strain rate: 0.018%/sec

Table 8.8 Ultimate load results for Batch 1 Series 3

Test number	Ultimate load (kN)	Test number	Ultimate load (kN)
43	64.57	48	62.55
44	64.46	49	62.57
45	61.33	50	64.93
46	62.10	51	62.21
47	60.36	52	64.41

Notes: Designation: Batch 2 Series 1
Objective: to investigate batch effects
Initial rope length: 2900 mm
Strain rate: 0.018%/sec

Table 8.9 Ultimate load results for Batch 2 Series 1.

Bed-down* in a single terminal (mm)	1.3
Overall modulus # (GPa)	116
Intercept on displacement axis of X-Y plot - initial slack (mm)	3.5

* measured after failure; hence, includes shock effects

based on loads between 10% and 90% NBL

Table 8.10 Typical additional results from strength tests on 6 tonne rope.

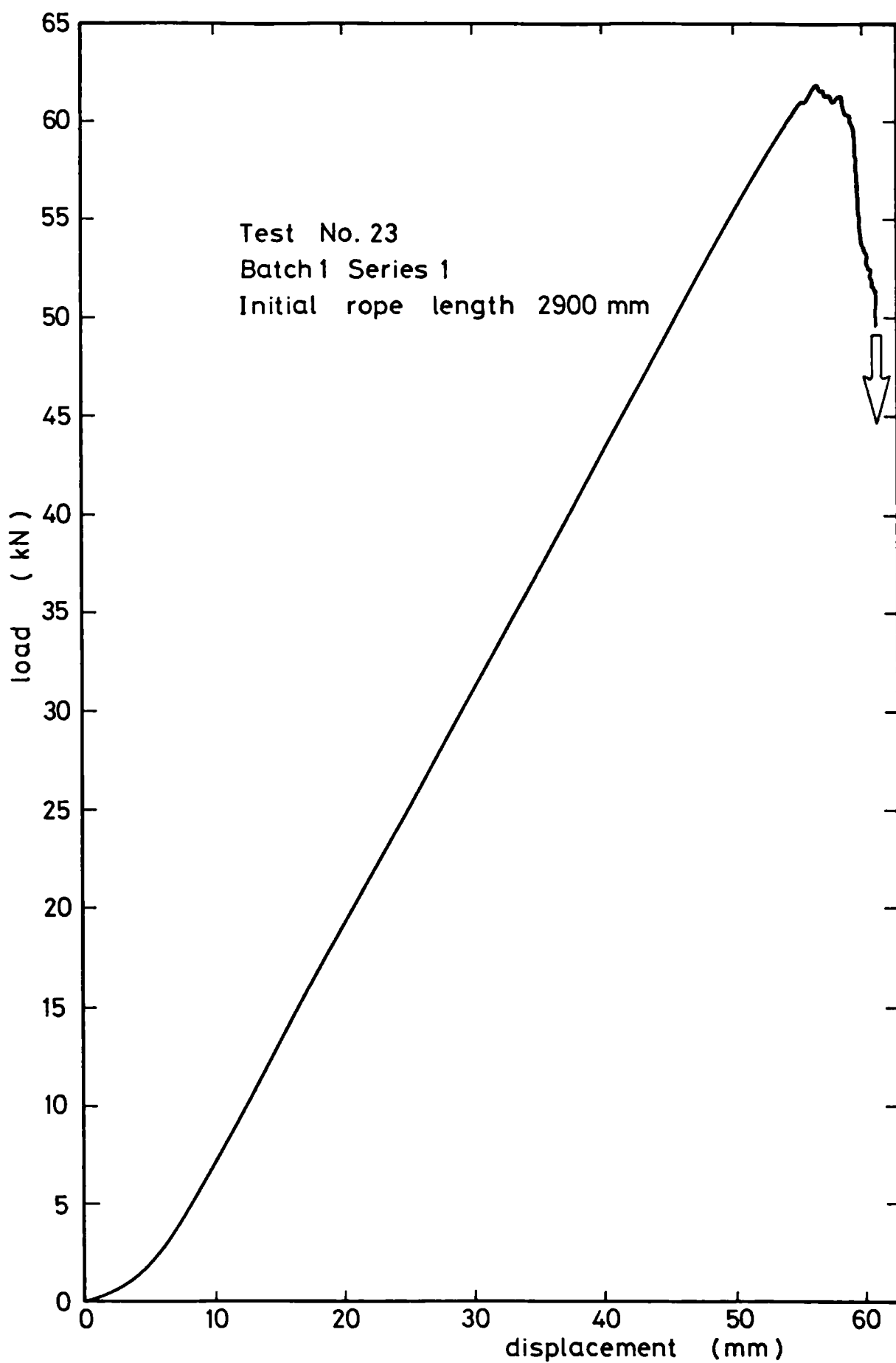


Figure 8.14 Typical X-Y plot for a rope loaded through to failure.

theory discussed earlier. For instance, larger ropes ought to be less variable (this follows from Equation 8.7) hence they would be more likely to exhibit a pronounced peak strength. The tensile strength efficiency of the larger ropes, on the other hand, ought to be marginally less in accordance with Figure 8.5; there is perhaps some evidence to suggest that this is the case (for example compare the results given in Table 8.11 (8.9.5) with those in Table 4.5). The first audible indication of yarn failure typically occurred at approximately 54 kN (a higher percentage of the NBL than observed in the 60 tonne rope tests); thereafter, yarns failed progressively.

Although the ropes were pulled handtight before testing to failure and there appeared to be no initial slack, an initial curvature in the X - Y plot was revealed for all ropes; an intercept on the X-axis of 3.5 mm was found to be typical. If the initial curvature is indicative of some yarns taking-up load before others (i.e. yarns contain varying degrees of slack) then this will have implications in terms of the tensile strength efficiency (8.10.1).

The majority of the rope failures were remote from the end-fittings. In some cases, however, the outer sheath pulled away slightly from one of the end-fittings to reveal a few broken yarns although on no occasion did a failure occur within a rope termination. Furthermore, only in one test (Test No. 32) was there any evidence that a failure close to an end-fitting had resulted in a reduced ultimate load.

The general appearance of the ropes of 2.9 metres length remained unaltered after they had been tested to failure; however, this was not true for the ropes of 10.9 metres length (Batch 1 Series 2). In the latter tests 'necking' was observed in the thermoplastic sheath, typically over a length of about 250 mm, indicating the point of failure within the rope; on one occasion the rope severed completely into two parts. The difference in behaviour is probably associated with the larger amount of strain energy released at failure in the tests on the longer ropes which is probably sufficient to cause deformation of the outer sheath.

8.9 Statistical analysis of the rope strength data

8.9.1 Distribution determination (Batch 1 Series 1)

To analyse any data, a statistical distribution must first be assumed, then some criterion of adequacy must be applied to determine whether the data are adequately described by that assumed distribution. Daniels (1945), Coleman (1958), and others, showed that the strength of a large bundle of fibres theoretically ought to be normally distributed. If comparisons between the theoretical and experimental strengths of Parafil ropes are to be regarded as valid, therefore, then a necessary precondition is that the distribution of rope strength is Gaussian.

Assume, therefore, that the strength data (Table 8.6) follows such a distribution. Figure 8.15 indicates a plot of the cumulative distribution function versus tensile strength drawn on normal probability paper; the plot should be linear if the distribution is normal. The scatter in the data makes it difficult to determine whether the plot is adequately linear; hence when this test of compliance is used some doubt remains as to the adequacy of the normal distribution to describe the strength data.

Further indications of whether data follow a normal distribution may be obtained by calculating the coefficients of skewness and kurtosis. The coefficient of skewness is a measure of the lack of symmetry in a distribution; its value for a perfectly symmetrical distribution (i.e. Gaussian) is zero. The coefficient of kurtosis is a representation of the 'peakedness' in a distribution; for a normal distribution it assumes a value of 3. These coefficients have been calculated for the data from Batch 1 Series 1 and found to be 0.34 and 3.10 respectively (the method of calculation is to be found in standard texts, e.g. Davies and Goldsmith 1977). The departures from the 'perfect' values are small and so the adoption of the Gaussian distribution to describe the data seems appropriate. Interestingly, the coefficient of skewness calculated for a sample of observations from a perfectly symmetrical distribution will not be exactly zero; in fact, there is a 5% probability that the coefficient of skewness for a sample of 30 observations from a normal distribution will be as high as 0.662 (Pearson and Hartley 1970).

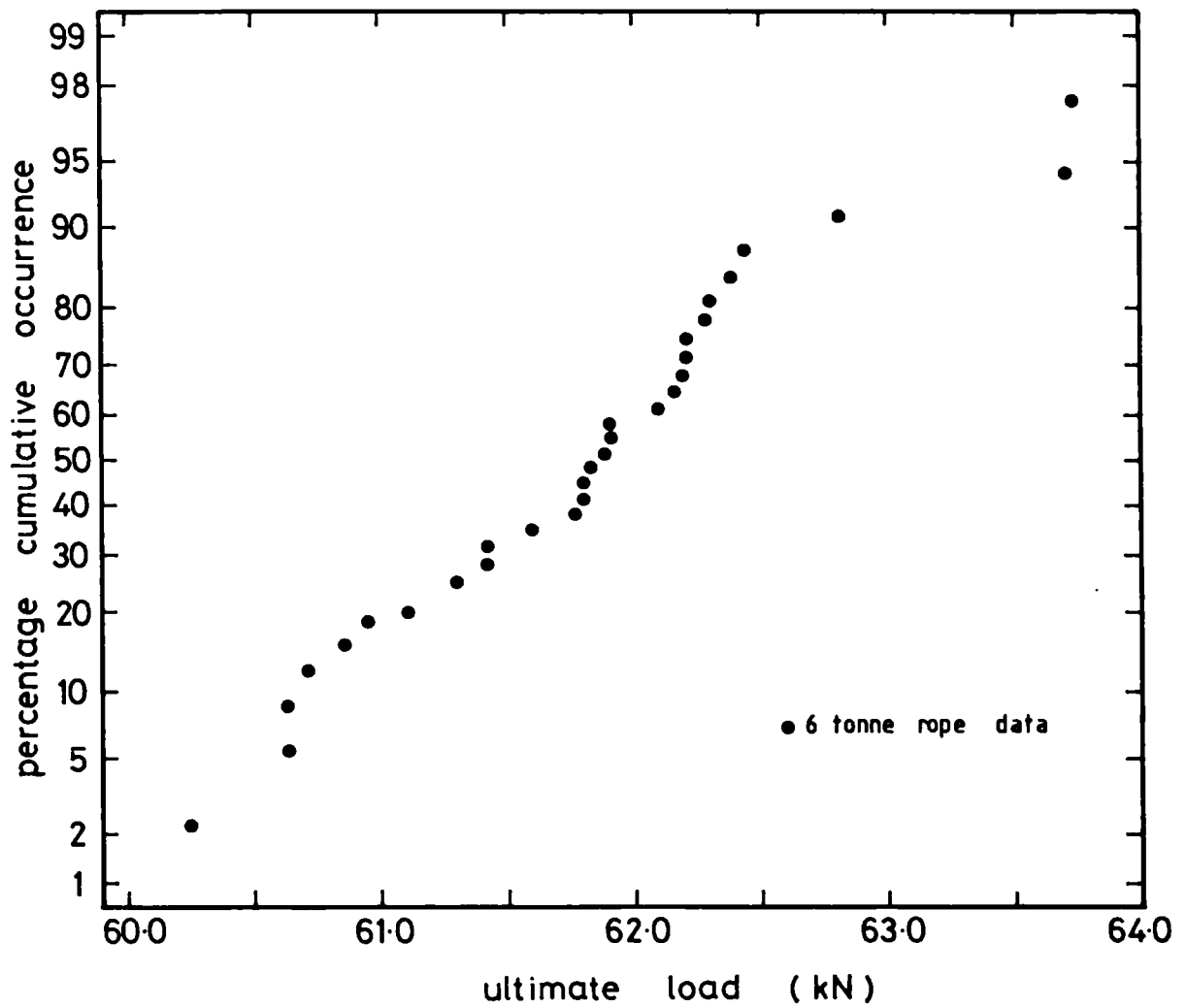


Figure 8.15 Cumulative distribution for rope strength drawn on normal probability paper (Batch 1 Series 1).

The normal distribution parameters may now be calculated. For the thirty ropes tested in Batch 1 Series 1 the sample mean strength ($\bar{x}$) and the sample standard deviation (s) are 61.81 kN and 0.82 kN respectively. Since the sample size is large the population standard deviation is approximated by the sample standard deviation and the mean of the population is most appropriately determined using the standardised normal distribution (commonly known as the z-distribution). The range for the mean of the population (μ) at a 95% level of confidence is found to be:

$$61.52 \leq \mu \leq 62.10 \quad (\text{kN})$$

8.9.2 The effect of length (Batch 1 Series 2)

The observed mean ultimate load in this series of tests was 60.86 kN which is significantly lower (statistically) than the mean of the shorter ropes. The reduction is probably attributable to the low ultimate load observed in Test No. 32 (58.74 kN) where failure occurred close to an end-fitting. The mode of failure in this particular test is probably a sufficient reason for rejecting this result but more formally, using a simple method to determine outliers (Davies and Goldsmith 1972) it can be shown that the probability of observing an ultimate load as low as 58.74 kN is less than 0.05; hence, the result may confidently be rejected.

The mean of the remaining five results is 61.28 kN which is still slightly lower than the sample mean of the main distribution. However, if it is assumed that the standard deviations of the two populations are equal then it can be stated that at a 95% level of confidence the effect of rope length on strength is statistically insignificant. (Theoretically, the standard deviation for the sample of longer specimens ought to be slightly smaller than that for the main distribution in accordance with 8.3.5; the difference, however, is negligible. Incidentally, a smaller sample standard deviation for the longer specimens was observed in practice - see Table 8.11.)

8.9.3 The effect of stretching ropes around a deflector (Batch 1 Series 3)

The mean strength observed during the deflected rope tests was 61.60 kN which again is marginally less than that of the main distribution (61.81 kN). However, at a confidence level of 95% the observed reduction in strength is not statistically significant.

8.9.4 Batch effects (Batch 2 Series 1)

The mean strength observed when testing ropes from a second batch (Batch 2) was 62.95 kN which is higher than for Batch 1. Indeed, the increase in strength is statistically significant; at a 95% level of confidence the strength of Batch 2 is greater than Batch 1 by an amount ranging from 0.55 kN to 1.72 kN.

A difference in strength between batches is perhaps surprising given that each rope is comprised of yarns from many different spools; the actual difference observed, however, while statistically significant, is still small, and represents a difference in terms of the mean strength of the main distribution of between 0.9% to 2.8%.

8.9.5 Statistics summary

A summary of the statistics for the tensile strength of 6 tonne Type G Parafil ropes is presented in Table 8.11.

	Designation			
	Batch 1			Batch 2
	Series 1	Series 2	Series 3	Series 1
number of tests	30	5#	6	10
sample mean breaking load $\bar{x}$ (kN)	61.81	61.28	61.60	62.95
sample standard deviation, s (kN)	0.82*	0.41	0.73	1.56
difference in mean breaking load which would be considered statistically significant (kN)	--	0.77	0.72	0.58
statistically significant difference in mean breaking load observed?	--	no	no	yes
evaluation of difference at 95% confidence level, $\Delta\mu$ (kN)	--	--	--	$0.55 \leq \Delta\mu \leq 1.72$

* approximately equals population standard deviation

Test 32 discounted (8.9.2).

Table 8.11 Summary of the strength statistics for 6 tonne Type G Parafil ropes.

8.10 Comparisons between experimental and theoretical behaviour

8.10.1 Tensile strength efficiency.

The theoretical tensile strength efficiency for a Type G rope of 2.9 metres length containing a large number of yarns is 0.85 (Table 8.1). This value should be compared with the experimentally observed tensile strength efficiency of 0.70 determined using Equation 8.10:

$$\text{experimental efficiency} = \frac{\text{mean rope breaking load}}{\text{rope area based on mean yarn size}} \div \text{mean strength of yarns} \quad 8.10$$

where: mean rope breaking load = 61812 N (Batch 1 Series 1)
 mean yarn size = 2563 decitex
 mean yarn strength = 2673 MPa.

There is a significant discrepancy between the theoretical and experimental tensile strength efficiencies. Three possible reasons for this discrepancy could be:

- (A) the strength of the rope is affected by the presence of initial slack (Figure 8.14)
- (B) Parafil ropes do not obey the equal loadsharing rule
- (C) There are deficiencies in the theory governing the strength of bundles.

These three possible causes will now be investigated.

A. The effect of initial slack

If a bundle of fibres (or yarns) containing varying amounts of slack is stretched then some fibres will begin to carry load before others. Consequently, the fibres which contained only small amounts of initial slack would probably be amongst the first to fail - load would be transferred to the surviving fibres and the strength of the bundle would be less than if no initial slack were present.

The presence of slack within a bundle of linearly elastic fibres can easily be detected by examining the load-displacement curve of the bundle for initial curvature; it may be interpreted from the initial

curvature that fibres are taking-up load at different strains. When the extension of a bundle equals the largest fibre slack present, the full stiffness of the bundle is reached; at this point the slope of the load-displacement curve for the bundle reaches a maximum and becomes constant.

The load-displacement curves for the Parafil ropes tested in this study contain some initial curvature which may be attributable to the presence of initial slack within the Kevlar 49 yarns. This initial slack could possibly explain the difference between the theoretical and experimental tensile strength efficiency of the 6 tonne Type G ropes.

Phoenix (1976) considered the effect of random fibre slack on the strength of a classical bundle of fibres; the slack was assumed to be uniformly distributed. Phoenix found that the introduction of random slack to a bundle of fibres resulted in large strength losses if the fibres were brittle, stiff and displayed low scatter in tensile strength; these properties are characteristic of Kevlar 49.

The theoretical reduction in tensile strength as determined by Phoenix (1976) was found to be dependent on the ratio of the standard deviation of the slack strain distribution to the fibre failure strain. (The slack strain of a fibre is defined as the slack divided by the length of the fibre). Figure 8.16 shows the theoretical effect of fibre slack on the tensile strength of the Kevlar 49 preproduction fibre (PRD-49); the family of curves indicates the severe reduction in strength which results if large amounts of slack strain are introduced. The reduction, R_s , may be determined using Equation 8.11 (Phoenix 1976):

$$R_s = 1 - \frac{\phi_s^2 \Gamma(1 + 1/\alpha) \alpha^{1+2/\alpha}}{2} \quad 8.11$$

where: R_s is the strength reduction factor due to slack

ϕ_s is the ratio of the standard deviation of the slack strain distribution to the mean of the fibre failure strain

α is the shape parameter of the Weibull fibre failure strain distribution

Γ is the standard gamma function.

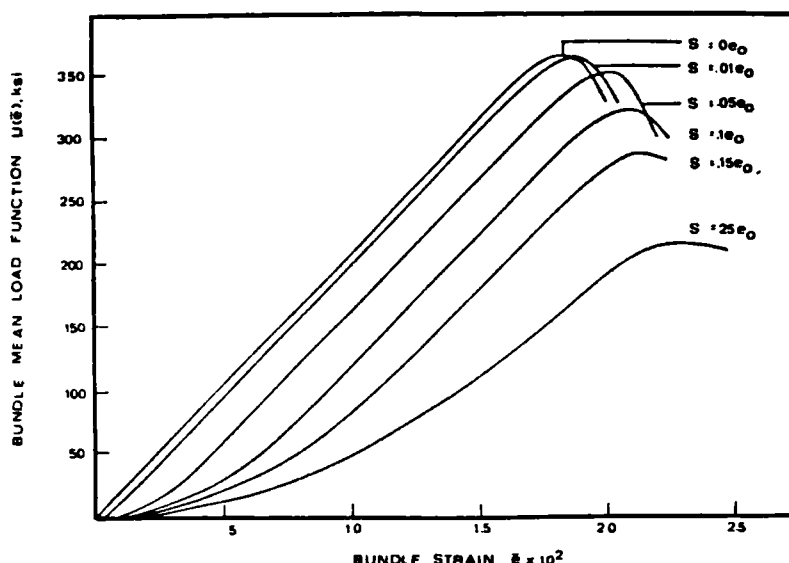


Figure 8.16 The theoretical effect of initial fibre slack on a bundle of PRD-49 fibres (Phoenix 1974).

A simple method of determining the standard deviation of the slack strain distribution (s) from an experimental bundle stress-strain curve is presented by Phoenix (1974); for a uniform distribution s is given by:

$$s = \frac{\theta_0}{2\sqrt{3}} \quad 8.12$$

where θ_0 is the strain at which the bundle attains a constant stiffness.

For the thirty tension tests performed on the 6 tonne Type G ropes (Batch 1 Series 1) varying degrees of initial curvature in the load-displacement plots were observed. From a consideration of the intercepts on the displacement axis (determined by extrapolating the linear part of the curve to the axis) the 'typical' load-displacement curve was isolated and the value of θ_0 found to be 0.275% strain. (Incidentally, the value of θ_0 for Parafil leads to a value of the standard deviation of the slack strain distribution, s , of $0.03e_0$ where e_0 is defined as the Weibull scale parameter of the Kevlar 49 yarn failure strain distribution. The shape of the Parafil stress-

strain curve (Figure 8.14) may be compared with the theoretical stress-strain curve for a bundle of the preproduction PRD-49 fibres having the same value of s as shown in Figure 8.16; the shape of the two curves appears consistent.)

In equation 8.11, α represents the shape parameter of the fibre failure strain distribution which for a population of fibres of constant stiffness is the same as the shape parameter of the fibre failure stress distribution. For the Kevlar 49 yarns some variability in the stiffness of the yarns was observed; hence, as a precautionary measure, the distribution of the fibre failure strains was determined. Although the failure strains were measured to two significant figures only (when recorded as a percentage) a two-parameter Weibull distribution was found to adequately describe the data. The shape parameter, α , was found to be 37.1 which is reassuringly close to that for the failure stress distribution (34.6).

If the necessary mathematical substitutions are now made into Equation 8.11 the reduction factor for slack strain is found to be 0.97. Hence, when the initial slack strain is taken into account the theoretical tensile strength efficiency for Type G Parafil ropes becomes $(0.97 \times 0.85) = 0.82$. The difference between the theoretical and experimental efficiency remains high, therefore, (0.82 c.f 0.70) with the effect of slack being small.

B. Effect of the loadsharing rule

One of the fundamental assumptions made in the prediction of Parafil rope strength was that of equal loadsharing amongst surviving yarns. The assumption seemed appropriate for Parafil ropes since they consist of independent parallel yarns to which a high lubricity finish has been applied. However, an examination of some of the failed 60 tonne rope samples (4.5.3) revealed that some yarns had failed in more than one location (possibly due to friction); hence, an argument in favour of non-equal loadsharing or localised loadsharing could be developed. Owing to the discrepancy between the experimental strength efficiency and the efficiency based on equal loadsharing, localised loadsharing warrants, therefore, some consideration.

Localised loadsharing is based on the assumption that load

redistribution (from failed fibres) is concentrated on the fibres which are the immediate neighbours of the failed fibres; the use of this rule is appropriate for situations in which there is a strong local interaction amongst fibres due to friction or a binding matrix. It is probable that loadsharing in the majority of ropes lies somewhere between the two extremes of equal and localised loadsharing.

Pitt and Phoenix (1981) have investigated the influence of both loadsharing rules on the tensile strength of bundles. Owing to the analytical complexity of the local loadsharing case, however, a bundle of only nine fibres was considered. One of the main conclusions from the work was that equal loadsharing led to stronger bundles than local loadsharing and that the two rules represented the two extremes of the strength scale.

The dependence of the strength of bundles on the choice of loadsharing rule is shown in Figure 8.17. The adoption of the equal loadsharing rule does indeed give rise to increased strengths at low probabilities of failure but the median strengths for both rules remain approximately equal. Hence, since comparisons between the theoretical and experimental strength of Parafil have been based on mean strength, an explanation for the disagreement in terms of the choice of loadsharing rule cannot be offered.

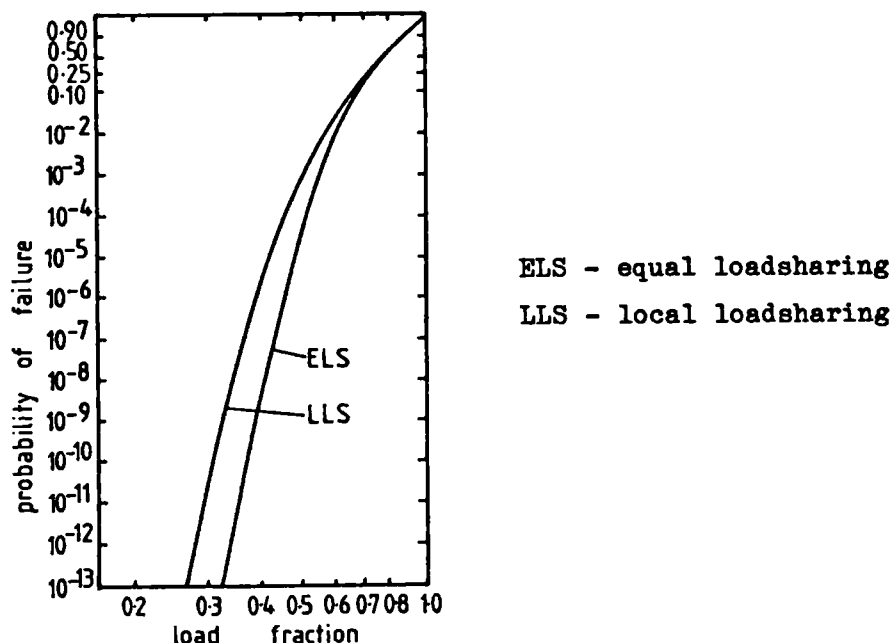


Figure 8.17 The effect of the loadsharing rule on bundle strength (Pitt and Phoenix 1985).

C. Validity of the theory.

Another possible explanation for the difference between the observed and the theoretical strength of Type G Parafil ropes is that there are deficiencies in the theory relating the strength of fibres to that of bundles. The validity of any theory is best proved by conducting experiments and comparing the results with the predicted outcome. However, while there is sufficient experimental proof to show that the Weibull distribution is most appropriate for describing the strength of fibres (e.g. Wagner et al. 1984 in respect of Kevlar 49) surprisingly little work appears to have been done to test the validity of the theory purporting to predict the strength of fibre bundles.

The lack of extensive experimental evidence is probably due to the conflicting disciplines of structural testing and theoretical statistics. However, Watson and Smith (1985) have analysed the work of Bader and Priest (1982) to assess whether the theoretical relationships between the tensile strengths of carbon fibres and bundles of carbon fibres are consistent with experimental results.

Watson and Smith (1985) estimated the strength of bundles of carbon fibres from the Weibull distribution describing the strength of single fibres. The same assumptions and procedures as described in this Chapter for bundles of Kevlar 49 yarns were followed; the results are shown in Table 8.12. For fibres of 20 mm length there was remarkable agreement between the theoretical and experimental bundle strength, as well as for the coefficient of variation. However, the good agreement did not extend to bundles of different lengths. An explanation for the discrepancy offered by the investigators was that there may have been some physical interaction between the fibres to invalidate the theory; however, the work of Pitt and Phoenix (1981) just described appears to rule-out this explanation.

Watson and Smith (1985) also conjectured that the variation in fibre diameter along the length of a single fibre could be important when assessing bundle strength although no data on this feature was provided. Wagner et al. (1984), however, found that there was considerable scatter in the diameter of a single fibre of Kevlar 49 when measured at various positions along its length and that it was

important to take this into account when determining tensile strengths. The variation in Kevlar 49 yarn diameter with length was not measured in this investigation but it would undoubtedly be less severe than for a single fibre; however, it may still be sufficiently large to provide a tentative explanation for the difference between the theoretical and experimental strength of Type G Parafil ropes.

Length of fibres (mm)	Theoretical mean μ^*	Theoretical coefficient of variation σ_N^*/μ^*	Observed mean	Observed coefficient of variation
5	2.19	0.014	1.92	0.016
20	1.71	0.014	1.68	0.015
100	1.28	0.014	1.58	0.012
200	1.14	0.014	1.38	0.007

Table 8.12 A comparison between theoretical and experimental strength parameters for bundles of carbon fibres (Watson and Smith 1985).

8.10.2 Variability in tensile strength

Phoenix (1978) was able to show using bundle theory that the coefficient of variation of stress-rupture failure times of a large bundle of the LLNL impregnated Kevlar 49 yarns was considerably less than for individual yarns. For this reason, it was stated (7.10) that the stress-rupture forecasts for Parafil based on LLNL pressure vessel data would probably be conservative. Using the same theory Phoenix (1978) also determined the ratio of the coefficients of variation of tensile strength for a bundle and single yarn. Remarkably, this ratio was numerically equal to the ratio of stress-rupture lifetime coefficients. Phoenix (1978) did not try to formally relate the stress-rupture ratio to that for tensile strength but he implied that there was some connection.

The coefficient of variation observed in the rope strength tests (Batch 1 Series 1) was 0.013; on the basis of the strength variability observed in the Kevlar 49 yarn tests a coefficient of variation of 0.013 was also predicted using bundle theory (8.3.4). Hence, there is excellent agreement between the experimental and theoretical values. The agreement on this issue is of greater consequence than the disagreement over the issue of actual strength since, assuming Phoenix's correlation to be repeatable (or at the very least, the trend) it can be inferred that the lifetime predictions for Parafil ropes, based on Kevlar 49 yarn stress-rupture tests, are indeed likely to be conservative. If the variability in rope strength had significantly exceeded that predicted theoretically then criticism of the use of yarn lifetime data to determine safe operating stresses for Parafil ropes (Chapter 7) would be justified.

8.10.3 Conclusions

There is excellent agreement between the experimental and theoretical variability of tensile strength of Parafil ropes. The agreement is reassuring in terms of the implications for stress-rupture (8.10.2).

However, there is considerable discrepancy between the experimental and theoretical tensile strength efficiencies. It was found that the presence of slack within the yarns of the rope could account for one-fifth of the discrepancy but explanations for the remainder continue to be tentative. Surprisingly, little effort has been spent in trying to validate the theory experimentally and so it is possible that the remainder of the discrepancy is due to shortcomings in the theory itself. Another possible explanation, that requires practical investigation, is that the yarn diameter varies with length resulting in narrow (and therefore) weaker cross-sections.

CHAPTER 9: THE TESTING OF A PRESTRESSED CONCRETE BEAM INCORPORATING A PARAFIL ROPE AS A PRESTRESSING TENDON

9.1 Introduction

The behaviour of prestressed concrete members containing steel tendons when subjected to flexure is well documented (see for example Abeles and Bardhan-Roy 1981). There is little reason to suppose that prestressed concrete members containing Parafil Type G tendons would behave much differently since stresses within the concrete matrix are largely independent of the tensile material which is providing the prestress. Nevertheless, it was thought that a practical demonstration of Parafil's ability to provide the prestress to a concrete member (a beam for instance) would be of benefit for the following reasons:

- (1) it would demonstrate the feasibility of the proposed anchoring system
- (2) it would provide useful 'site' experience
- (3) it would enable the behaviour of a Parafil beam when subjected to normal and overload conditions to be closely studied
- (4) it would be of general research interest
- (5) it would be the first time that Parafil had been used in this manner and would thus generate considerable interest amongst the engineering fraternity
- (6) it would help to dispel some of the understandable scepticism of structural engineers towards new materials (and Parafil in particular).

Consequently, the first concrete beam to rely for its strength on the prestress provided by a tensioned Parafil Type G rope was constructed and tested as part of this research programme.

This Chapter begins with a discussion of the considerations given to the design of the beam. Detailed sections are devoted to the method of beam construction, the prestressing operation, and the test rig and instrumentation employed. The behaviour of the beam during two types of test is then described. The first test was designed to prove the adequacy of the beam and the applied prestress to cope with typical working loads. The objectives of the second test were to investigate the behaviour of the beam under excessive loads and to show that the

ultimate moment of resistance of the beam was significantly greater than the bending moment due to working loads.

9.2 Design considerations

Existing laboratory formwork, designed primarily for beams of rectangular cross-section and about 5 metres in length governed the overall dimensions of the beam. The formwork was adapted to enable an I-shaped beam to be constructed; this shape was chosen since I-beams are structurally more efficient and more common in practice than rectangular beams.

This particular beam contained a single, straight, unbonded, 60 tonne Type G Parafil tendon stressed initially to 70% of its nominal breaking load. This rope stress is undoubtedly too high for a long-term application due to restrictions associated with stress-rupture (see Table 7.5) but was perfectly safe for a beam which was to be tested to failure shortly after the application of the prestress.

Parafil ropes as presently made do not bond to concrete. Tests predictably show that slip occurs between the core and the sheath and also between the sheath and the concrete (Guimares 1985). Hence, Parafil ropes will be used as unbonded tendons in prestressed concrete applications. The lack of bond is advantageous, however, as it will limit the additional strain in the Parafil when the member is subjected to overload conditions. This is especially important given the brittle mode of failure of Parafil (Figure 4.7).

The general arrangement of the beam is shown in Figure 9.1. The size of the bottom flange was determined from elastic design considerations and also from the need to provide sufficient concrete cover to the underside of the end anchorages. The depth of the top flange was chosen to give a suitable ultimate moment of resistance. The ultimate moment of resistance of the beam was determined using an iterative equilibrium method based on a rectangular stress block for the concrete and an assumed 10% increase in the tendon strain at failure; for a concrete strength of 40 MPa an ultimate moment of resistance of 132.4 kN-m was predicted. The prediction took into account the reduction in prestress due to stress-relaxation that would occur

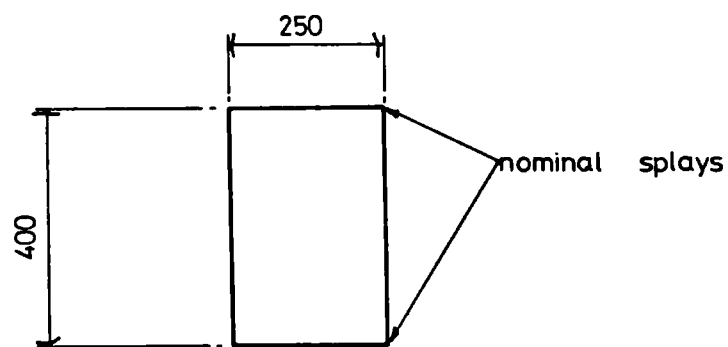
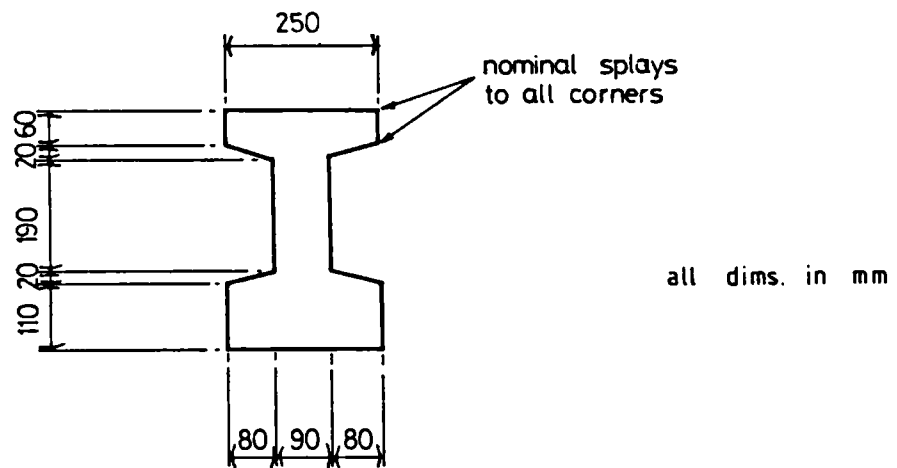
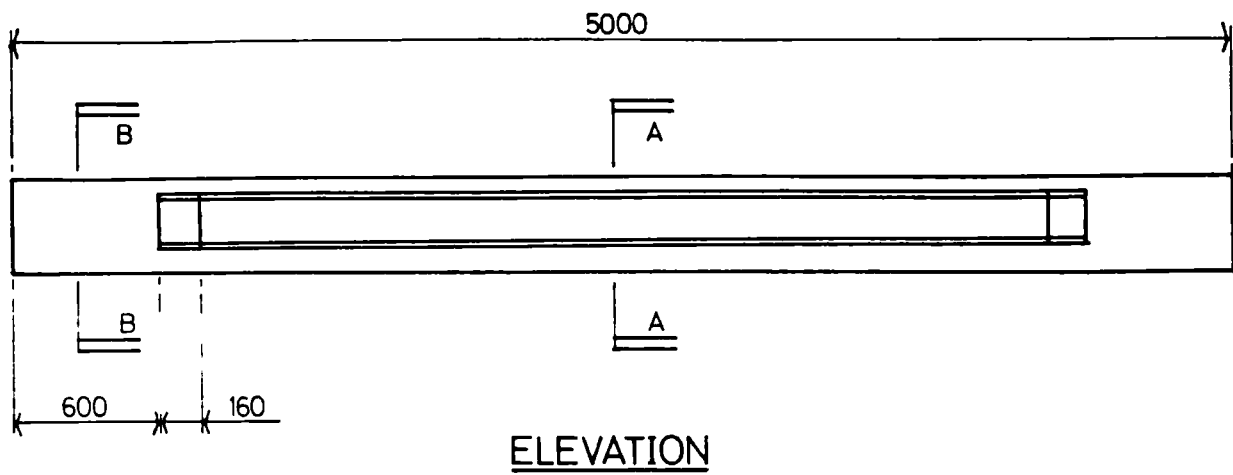


Figure 9.1 Beam general arrangement.

between the time of application of the prestress and the time of testing. An alternative method of determining the ultimate moment of resistance of unbonded prestressed concrete beams, validated by a large number of laboratory tests, is presented by Pannell (1969); when this method was used a very similar result was obtained.

To enable the end anchorages to be accommodated and so as to limit the anchorage bearing stresses the main I-section of the beam was splayed-out to a full rectangular section at the beam ends. The end-blocks were designed using the method of Zielinski and Rowe (as described in Abeles and Bardhan-Roy 1981); mild steel reinforcement in the form of vertical links was provided to resist the tensile splitting stresses.

A check on the design shear forces indicated that only a nominal amount of shear reinforcement was required throughout the beam. A small amount of mild steel longitudinal reinforcement was also provided to prevent cracking due to concrete shrinkage, to enable the shear links to be fixed, and to give the beam sufficient strength to support its own dead weight if necessary. Generally all beams containing unbonded tendons contain some longitudinal steel in order to provide increased ductility under overload conditions and hence warning of imminent structural collapse. Longitudinal reinforcement also ensures better crack distribution. Figure 9.2 indicates the Parafil prestressing and steel reinforcement details.

9.3 Beam construction

The beam is shown at progressive stages of construction in Figures 9.3 to 9.8.

Figure 9.3 shows the 60 tonne Parafil rope, with end terminals fitted, resting on the soffit formwork. The rope terminals were housed within end anchorages which in turn were screwed to the end formwork.

The prestressing duct consisted of a standard PVC pipe of 35 mm internal diameter (c.f. rope diameter of 31 mm). The pipe was cut into two halves to facilitate fitting; Figure 9.4 shows one half of the duct (containing the rope) temporarily supported with the other half positioned ready for gluing. Also shown in Figure 9.4 are the

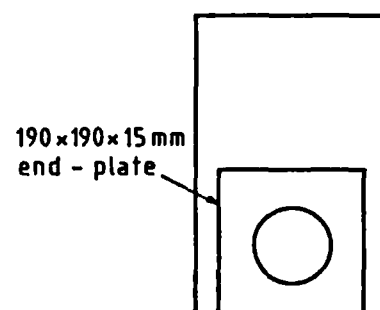
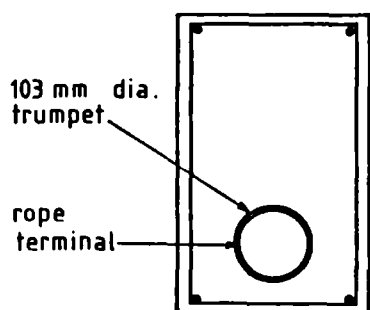
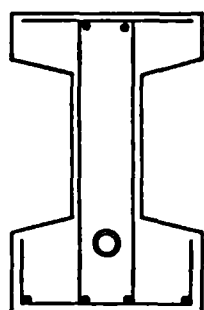
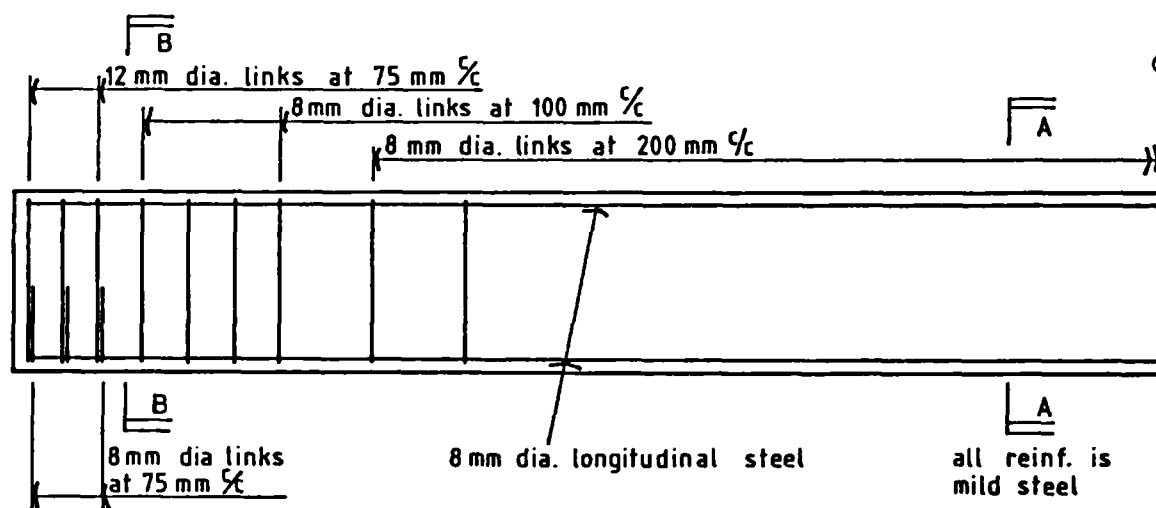
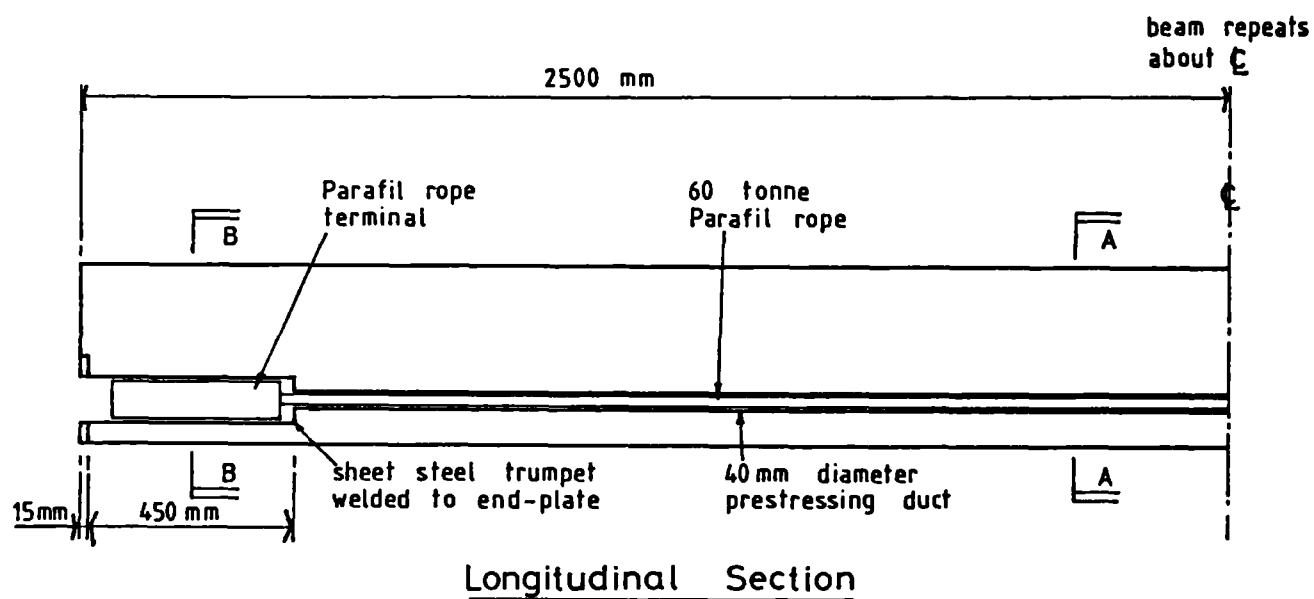


Figure 9.2 Parafil prestressing and steel reinforcement details.

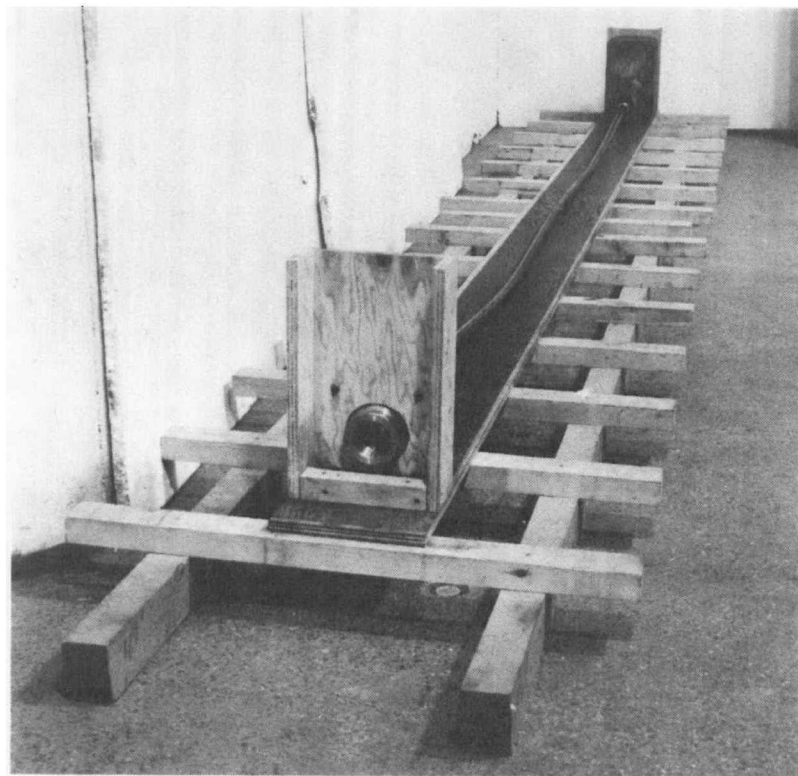


Figure 9.3 View of rope, soffit and end shutters.

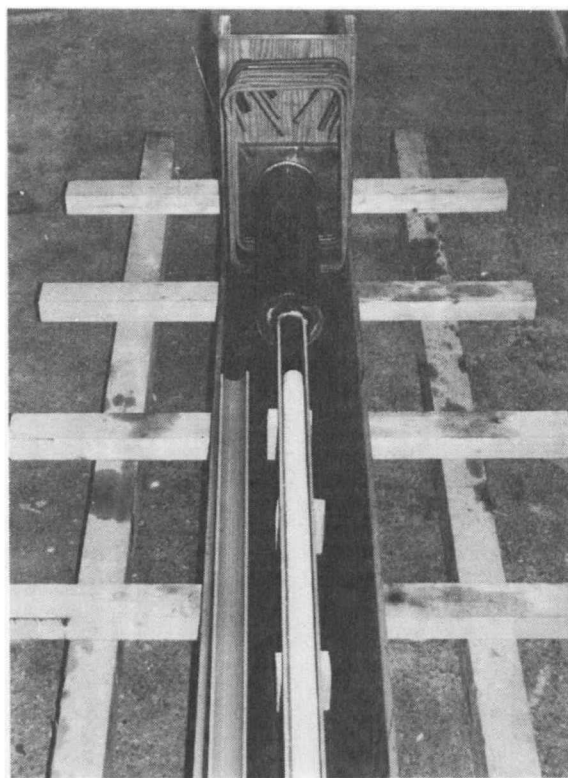


Figure 9.4 Rope is wrapped in PTFE tape before fixing duct.

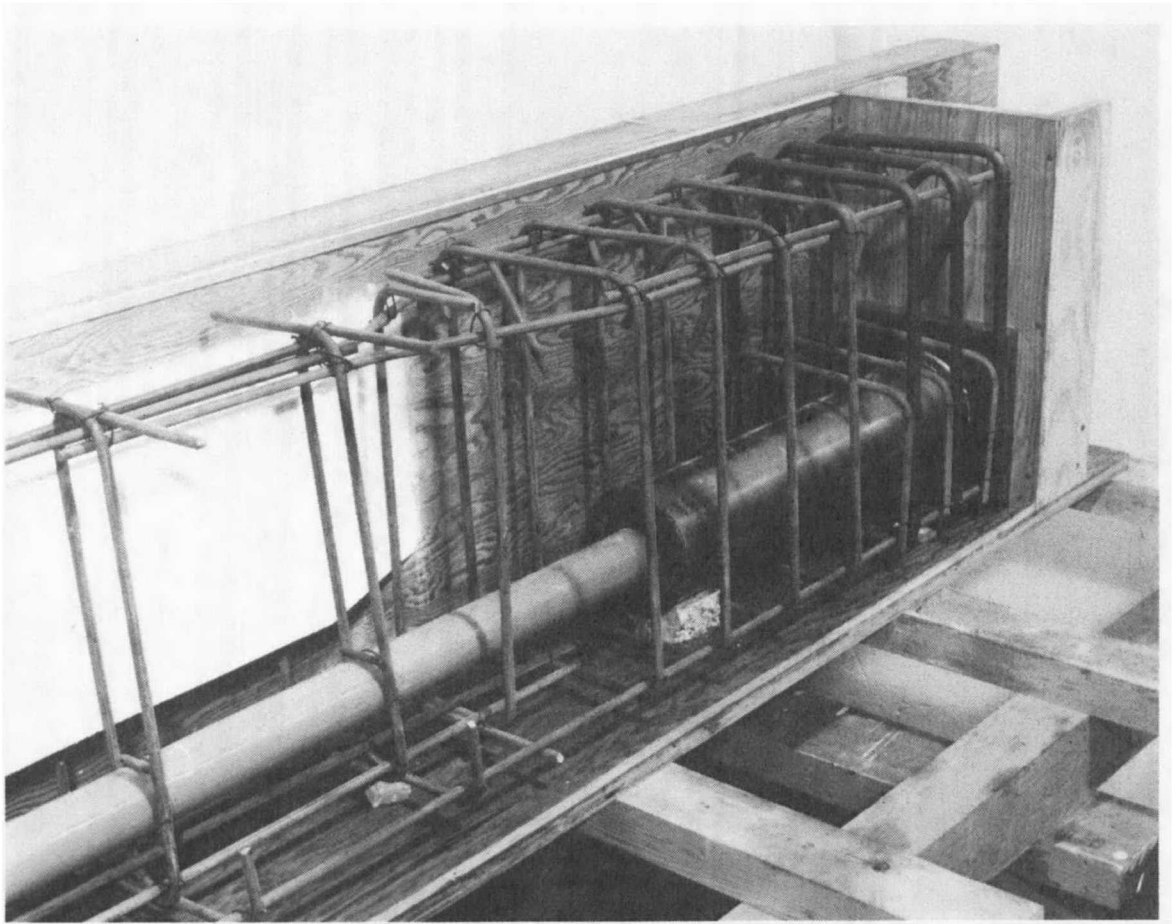


Figure 9.5 View of anchorage, end-block reinforcement, and side shutter.

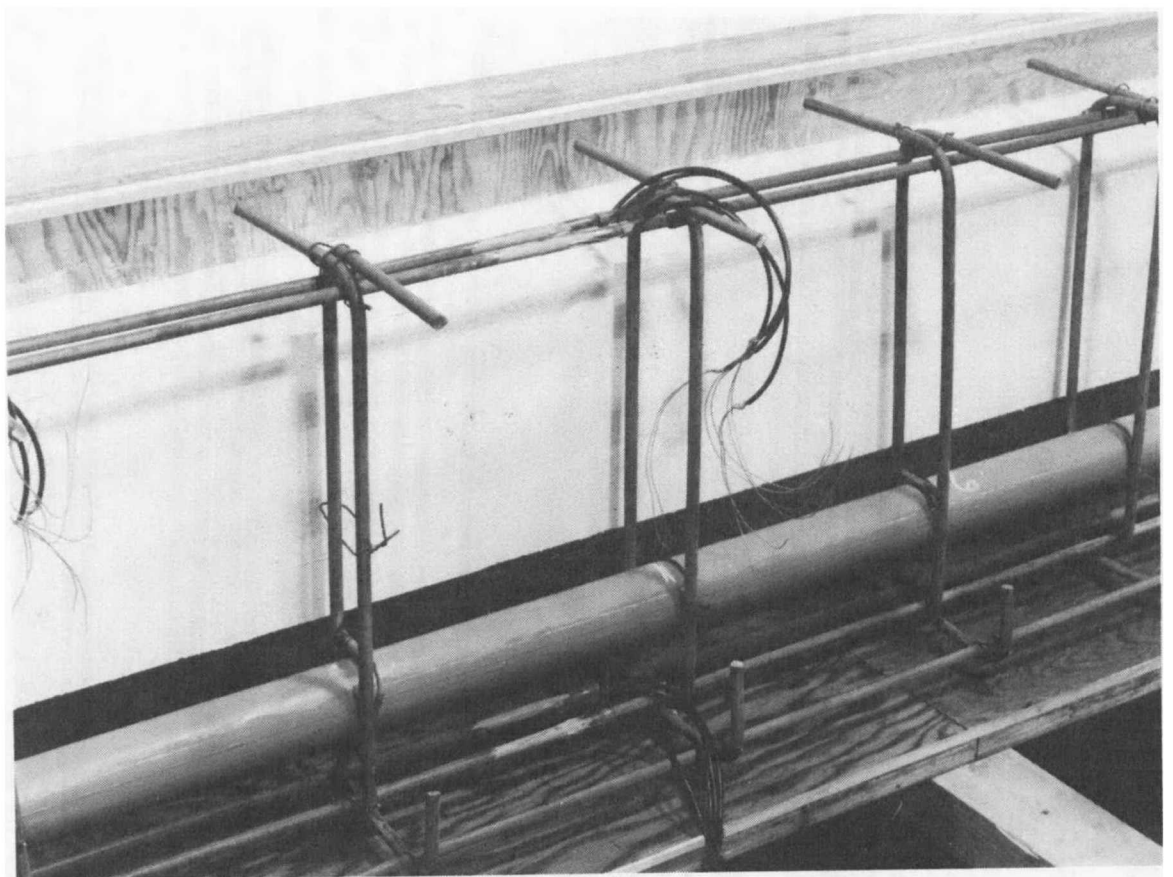


Figure 9.6 Details of midspan showing strain-gauged longitudinal steel.

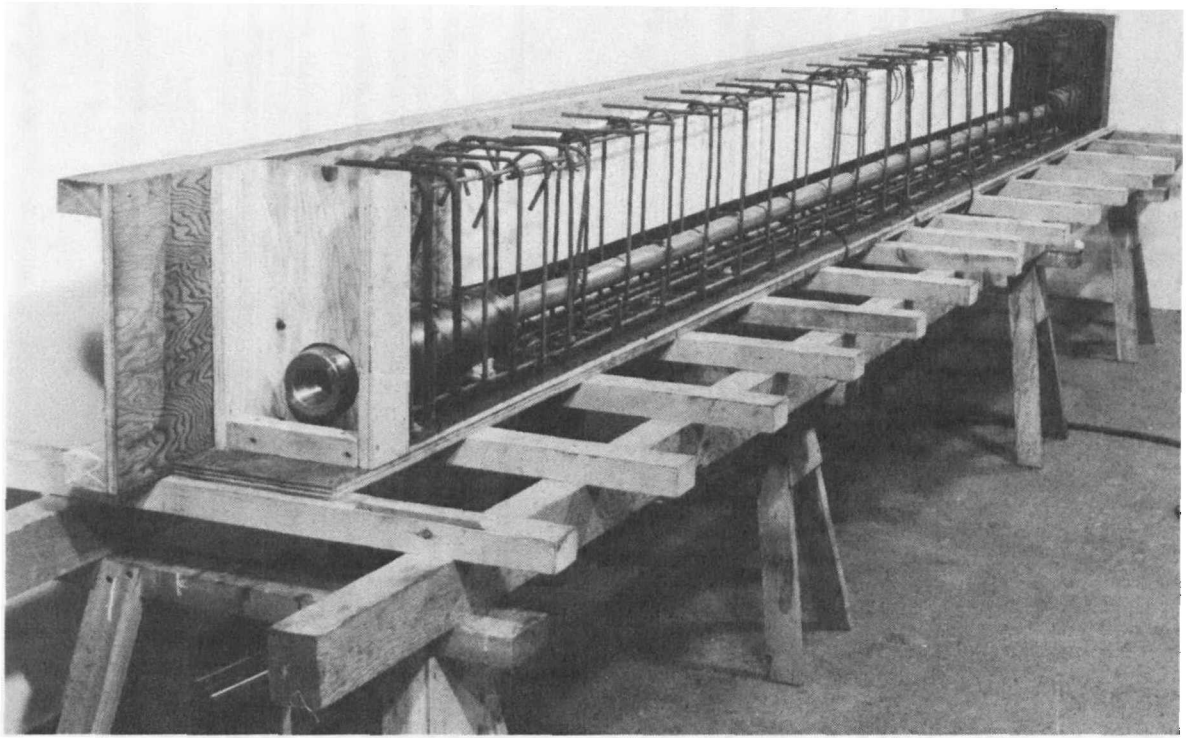


Figure 9.7 Overall details prior to casting.

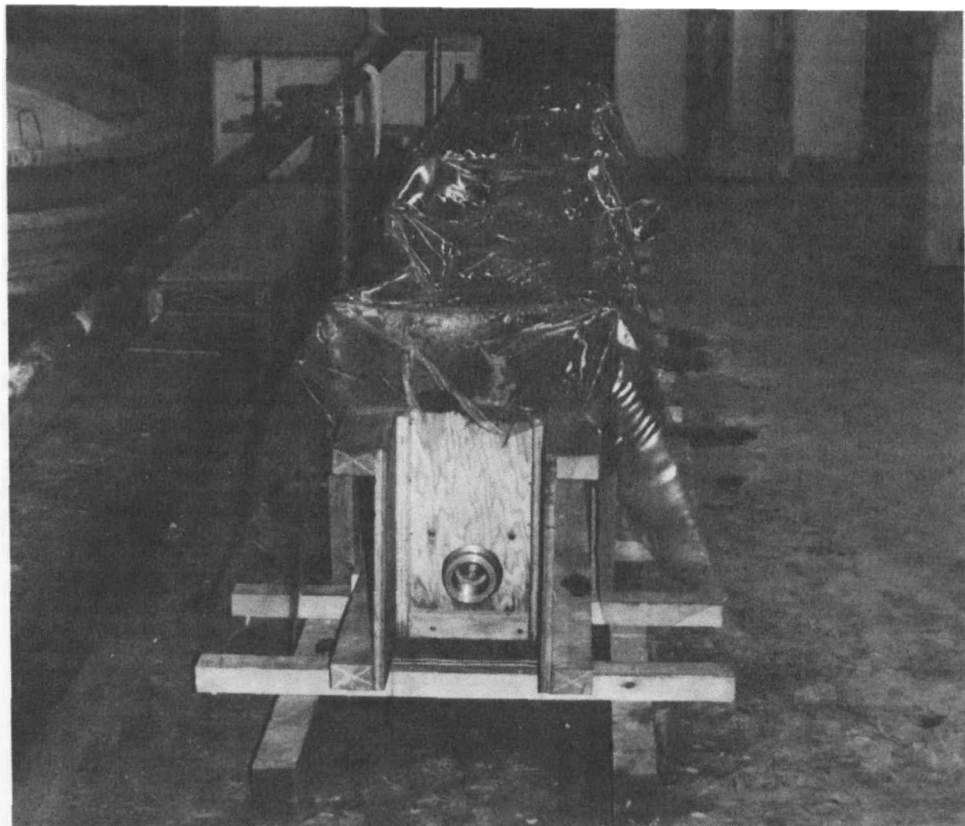


Figure 9.8 Beam is cured under damp hessian and polythene sheeting.

mild steel vertical links positioned prior to fixing in the end-blocks.

Since the coefficient of friction between the polyethylene outer sheath of the rope and the plastic prestressing duct was believed to be quite high the rope was wrapped in PTFE tape (see Figure 9.4). The tape was adopted for two reasons:

- (1) to ensure that the full initial prestress was transferred to the concrete and not reduced by frictional losses (thus allowing the full beam strength to be realised)
- (2) to prevent the build-up of high additional local strains in the rope during the ultimate tests which could have led to rope failure.

Figure 9.5 shows a close-up view of an end anchorage. The end anchorages consisted of a sheet steel cylindrical void former (to house the rope terminal) welded to a 15 mm thick end-plate designed to transmit the prestressing force to the beam. The rectangular links are shown closely spaced in the end block to prevent local bursting forces from cracking the surrounding concrete.

Also shown in Figure 9.5 are the details of the side formwork. The use of polystyrene sheets, cut to the required shape with a hot wire and glued to the original side shutters enabled the I-beam to be constructed.

Figure 9.6 indicates the details of the beam at midspan. The longitudinal steel at this position is strain-gauged as discussed in 9.5. The prestressing duct is shown supported off the vertical shear links with provision having been made to prevent duct uplift during casting due to buoyancy effects.

Figure 9.7 shows the beam prior to casting. A concrete mix was chosen to give a 28 day cube strength of about 40 MPa with sufficient workability to ensure complete filling and compaction of the bottom flange. The properties of the concrete employed are shown in Table 9.1 After casting, the beam was cured under damp hessian and covered with polythene sheeting for 10 days (see Figure 9.8) before the side and end shutters were stripped.

28 day cube strength	48.7 MPa (average of 3)
28 day tensile strength	3.0 MPa (average of 2)
40 day match-cured specimens:	
cube strength	57.5 MPa (average of 3)
tensile strength	3.9 MPa
initial tangent modulus, E	35800 MPa

Table 9.1 Concrete properties.

9.4 Prestressing operation

To avoid the possibility of inadvertently cracking the beam through early lifting, the beam was allowed to remain on the soffit formwork until the prestress had been applied. This is a precaution normally taken in commercial precasting yards.

The prestress was applied to the beam forty days after casting and just 20 hours before testing. Prior to applying the prestress the 60 tonne Parafil rope was first pretensioned to 60% NBL (in the usual manner) using the actual concrete beam as the reaction frame. One hour later, a prestress of 42 tonnes (70% NBL) was applied to the beam. The same method of stressing the rope and of transferring the rope stress to the beam was employed as for previous rope tests. However, some of the prestressing components (e.g. the locknuts and the trestle for the jack) were reduced in size to be consistent with the dimensions of the beam. The prestressing operation is shown in Figure 9.9.

Measurements of load, total rope displacement, spike bed-down in the dead-end terminal, midspan camber of the beam and strain in the longitudinal steel reinforcement were taken throughout these stressing operations (some results are shown in table 9.2). A comparison of the predicted strain distribution in the concrete due to the initial

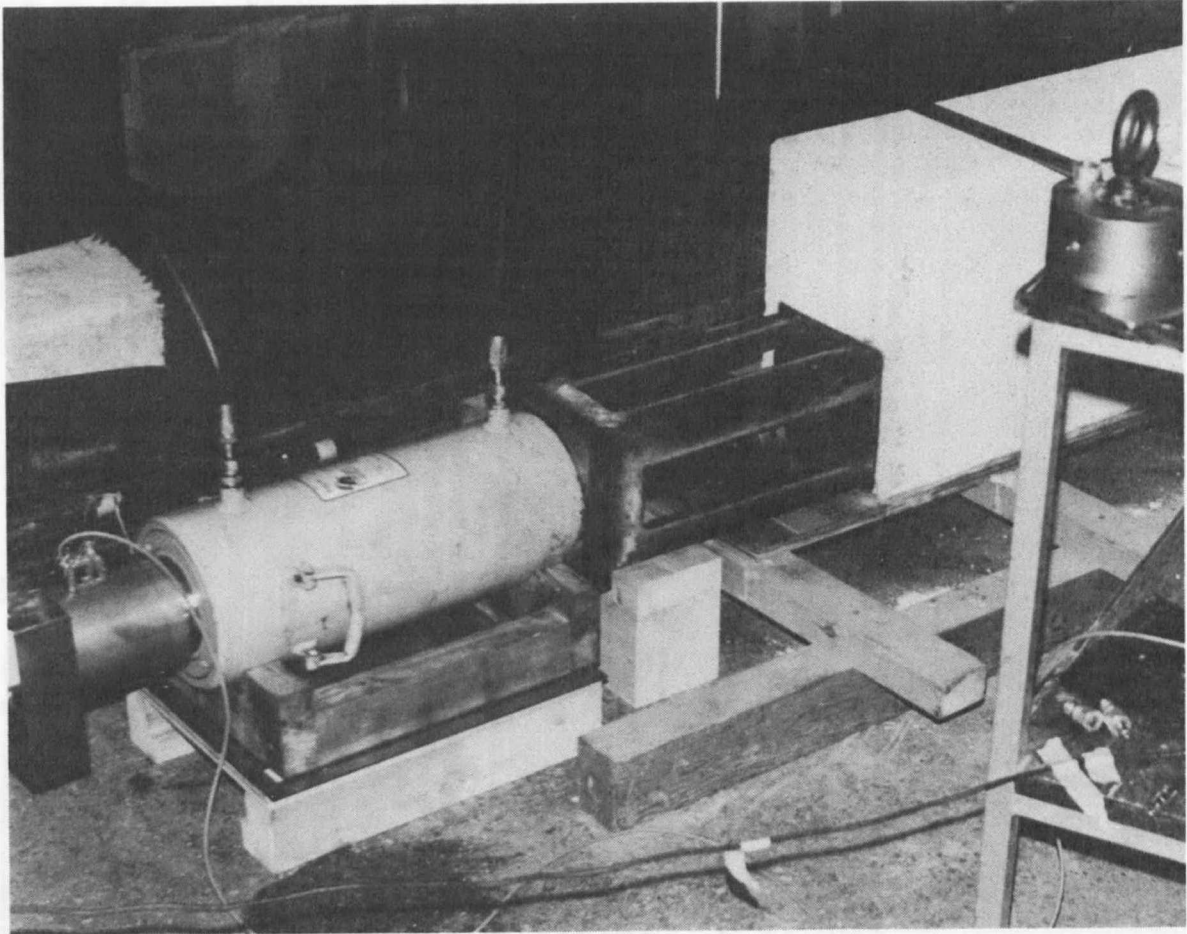


Figure 9.9 Prestressing operation.

prestress plus dead loads, with the observed distribution (from strain gauges) gave very good agreement, indicating a negligible loss of prestress due to friction between the rope and the prestressing duct.

After prestressing, safety devices were fixed to the protruding rope terminals (as shown in Figure 9.10) to prevent the terminals flying out in the event of failure in the rope. These straps were fitted loosely and would not have interfered with the test results. In practice safety straps would not be necessary and the end anchorages would appear extremely practical and unobtrusive as shown on Figure 9.11.

Rope used:	60 tonne Parafil Type G
Initial prestress:	415 kN (42 tonnes, 70% NBL)
Midspan camber:	1.9 mm
Total rope displacement (incl. bed-down etc):	60 mm at 415 kN
Time of prestressing:	40 days after casting
	20 hours before testing

Table 9.2 Prestressing data.

9.5 Test rig and instrumentation

The test rig is shown in Figure 9.12. The beam was simply supported on steel bearings bedded horizontally on concrete blocks to give a span of 4.572 metres. A 20 tonne Amsler jack, powered by an Amsler hydraulic cabinet, applied load to a steel spreader beam with loading points 1.1 metres apart and placed symmetrically about the concrete beam centreline. The use of a spreader beam ensured that the central region of the beam would be subject to pure flexure and that the possibility of local crushing resulting from a single point load would be considerably reduced. The jack was supported from a steel frame stressed to the laboratory floor with a force of 60 tonnes.

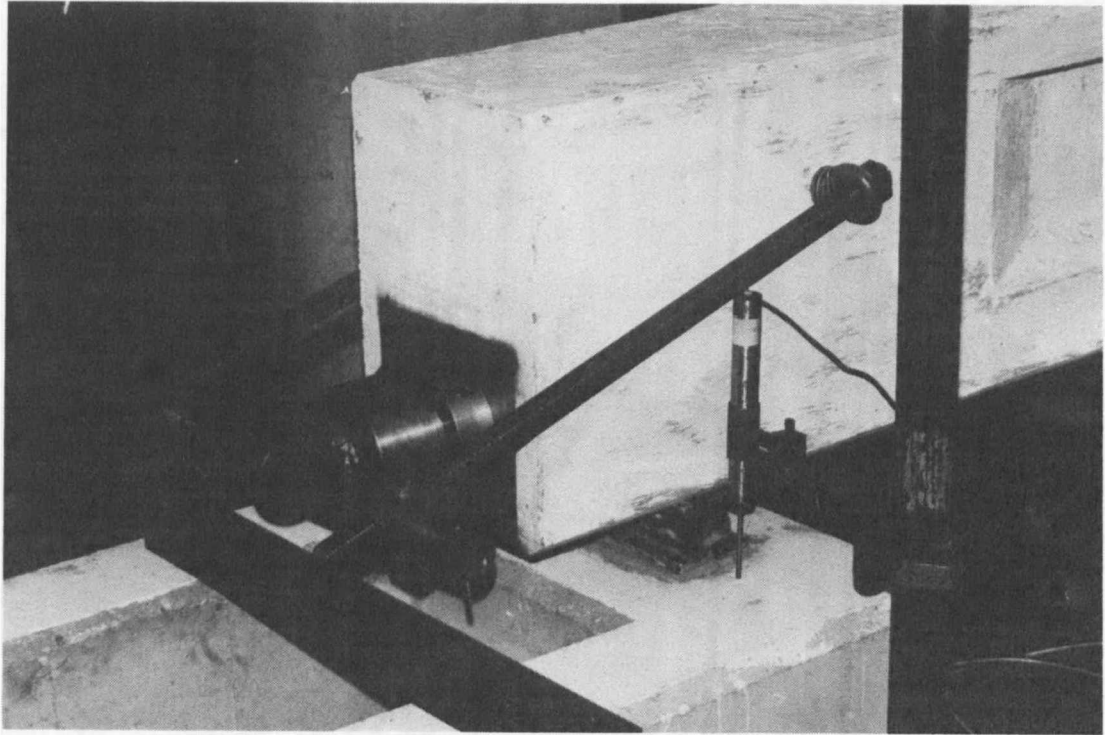


Figure 9.10 Temporary safety device.

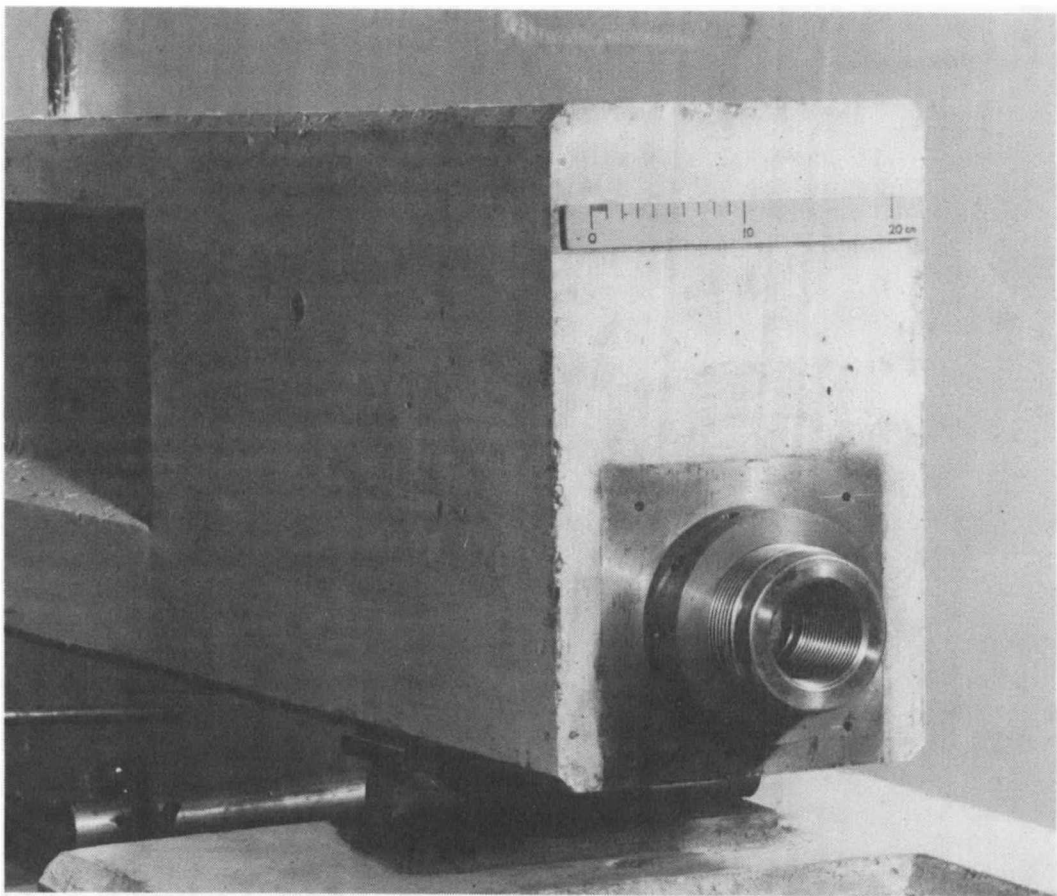


Figure 9.11 External view of end anchorage.

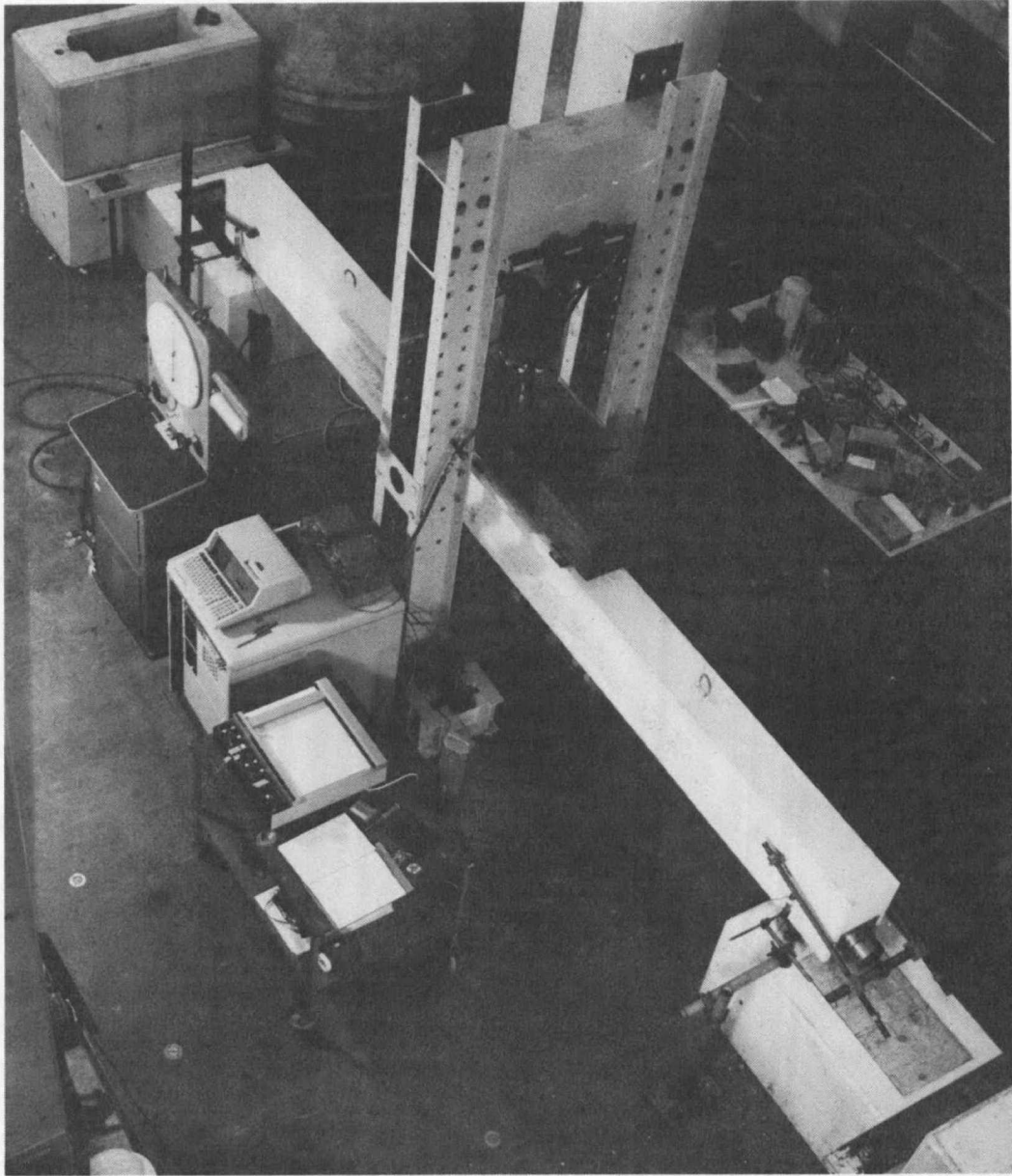


Figure 9.12 Overall view of test rig.

Test measurements were taken using the Hewlett-Packard data acquisition system controlled with specially written software. Three displacement transducers (LVDT's), one at each support and one at midspan, gave an accurate measurement of the net midspan deflection. Two additional LVDT's positioned in line with the spreader beam loading points, were used to enable the average curvature over the region of constant bending moment to be calculated.

Strain gauges (see Figure 9.6) were fixed to the longitudinal untensioned steel at three cross-sections of the concrete beam - at midspan and under the spreader beam loading points. They permitted the strain distributions and local curvatures under every loading condition to be accurately assessed and helped greatly in the analysis of results. Twenty-four strain gauges of 20 mm gauge length and 120 ohm resistance were used in all.

A pressure transducer connected directly to the Amsler cabinet was found to give satisfactory load measurement; the output voltage from this instrument and that from the midspan displacement transducer were used as input for an X-Y plotter to allow beam behaviour to be examined qualitatively as the tests proceeded.

9.6 Elastic test

A test was devised to prove the adequacy of the prestress provided by the Parafil rope and to demonstrate that the beam would behave satisfactorily under working loads. The test was based largely on the procedure laid down in the Department of Transport's Specification for Road and Bridge Works (1976) for testing pretensioned precast beams containing steel strand.

The intention of the test was to apply a load that would induce the allowable flexural tensile stress in the bottom fibre of the beam. Allowable flexural tensile stresses (as specified in codes of practice) are usually determined by applying a safety factor to the direct tensile strength of concrete; hence, the induction of this stress in the bottom fibre of the beam should be insufficient to cause even microscopic cracking, ensuring that the beam remains elastic and the response linear. Figure 9.13 illustrates the expected behaviour

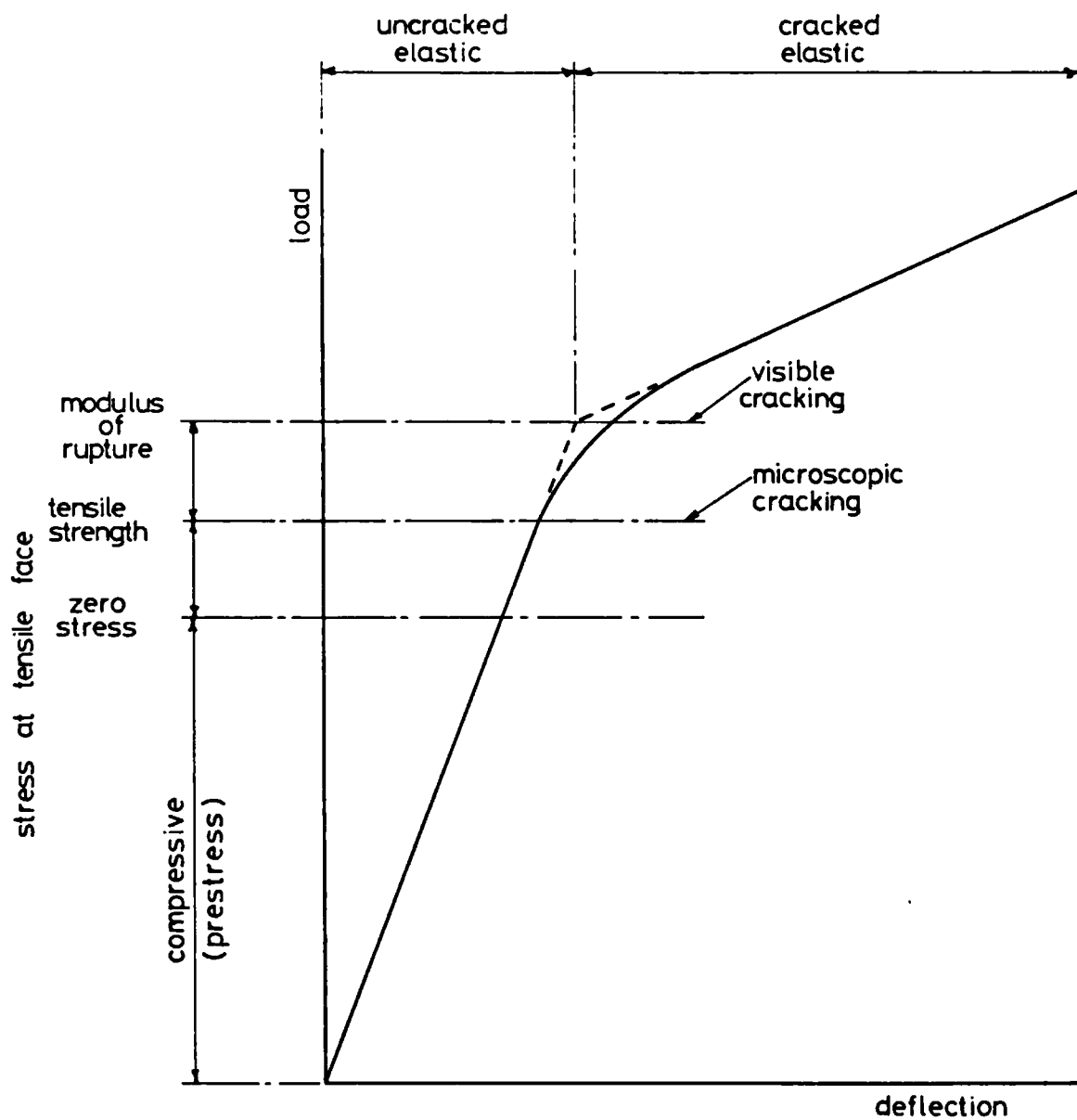


Figure 9.13 General behaviour of an unbonded prestressed concrete beam.

of unbonded prestressed concrete beams loaded in flexure; test loads are designed to ensure that a position is reached on this load-deflection curve near the end of the linear region. If the experimentally observed load-deflection curve were markedly non-linear or if at least 90% of the deflection were not recovered (upon removal of the test load) then this would suggest that the concrete had cracked and the beam would be deemed to have failed the test.

The 28 day cube strength of the concrete mix was found to be 48.7 MPa and on this basis the British Code of Practice for the structural use of concrete (CP110 1972) quotes an allowable flexural tensile stress of 2.5 MPa. The test load to produce this stress (9.7 tonnes) was applied gradually and maintained for 5 minutes. The load was then removed steadily and the elastic recovery of the beam was monitored. Five minutes after removal of the load 94% recovery of the midspan deflection had occurred. Furthermore the load-deflection curve, as shown in Figure 9.14, was essentially linear. The beam had, therefore, behaved satisfactorily (and entirely as expected) under working loads thus proving the adequacy of the prestress. Data and results relating to the elastic test are shown in Table 9.3.

Test load	95 kN (9.7 tonnes)
Test bending moment	82.5 kN-m
Midspan deflection	4.78 mm
Residual deflection 5 mins. after unloading	0.29 mm
Elastic recovery	94%
Assumed prestress loss prior to testing	6.25%

Table 9.3 Elastic test data.

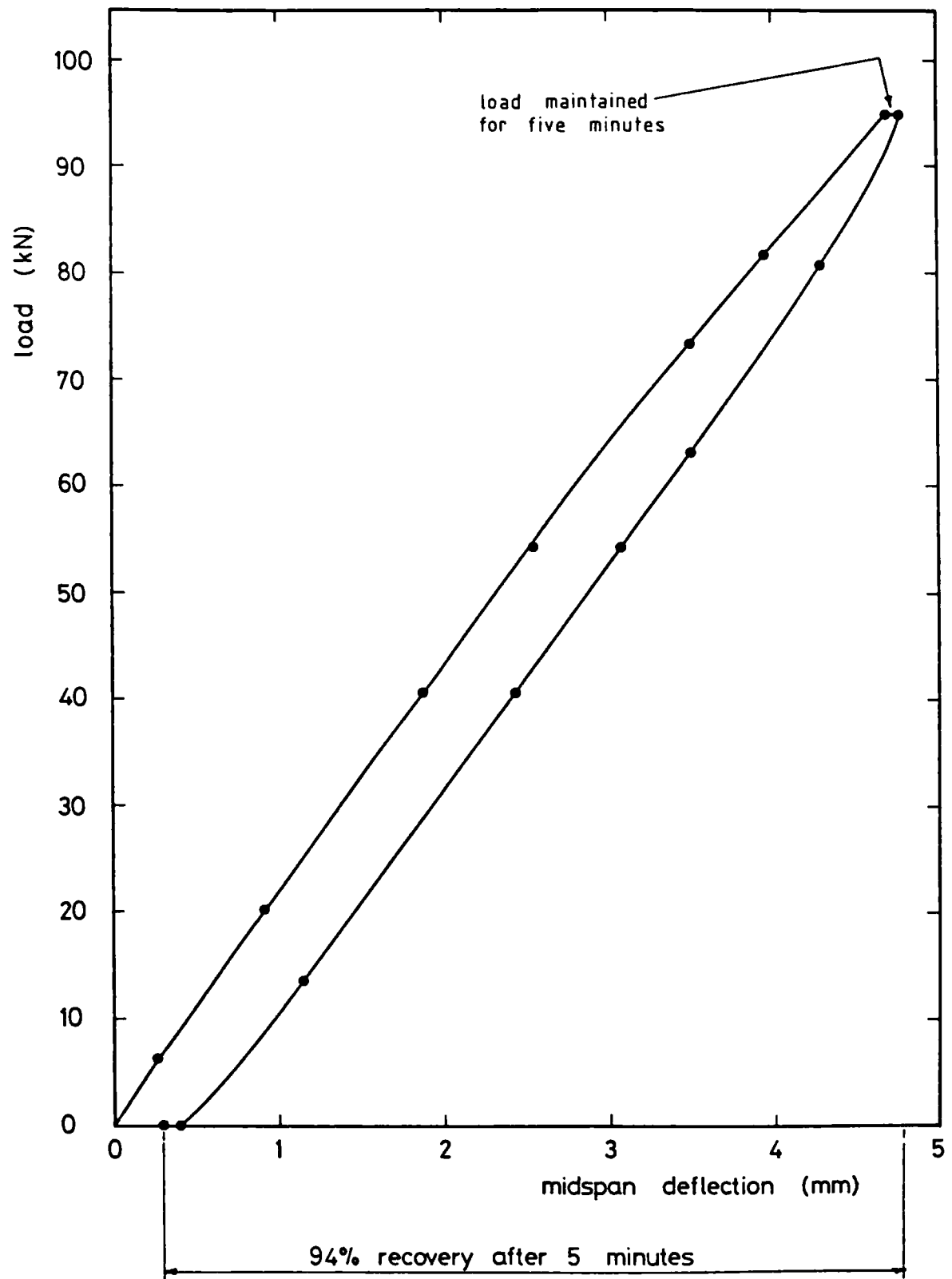


Figure 9.14 Elastic test load-deflection curve.

9.7 Ultimate test

One hour after completion of the elastic test (9.6) an ultimate load test was performed. The test was carried out using three increasing load cycles so that as much information ^{as possible} about the behaviour of the beam could be gathered. It is generally accepted that the effect of load-cycling in this manner is small and that the flexural behaviour of a beam when subjected to a monotonically increasing load may be inferred from the response of the beam to each successive load cycle.

Figure 9.15 shows a plot of the total load applied to the beam against the midspan deflection; Figure 9.16 gives a similar picture for bending moments plotted against curvature (averaged over the central region of constant bending moment). The ascending portion of the two plots attributable to the first cycle was found to remain linear for much larger loads than the next two cycles. This is because the tensile strength of the concrete had first to be overcome, the beam remaining linearly elastic until this had been achieved. The load-deflection curve was found to depart from linearity at around 107 kN on first loading which can be compared with the elastic test (9.6) during which a load of 95 kN had been applied. For further increased loads the load-deflection plot became decidedly curved and the first flexural crack appeared visible to the eye at around 120 kN indicating that the modulus of rupture of the concrete had been exceeded. A load of 136 kN was attained during the first cycle upon which the beam was gradually unloaded. The load-deflection curve was found to become linear during unloading once the prestress was able to close-up any cracks that had formed.

The load-deflection curve for the second cycle of loading became non-linear at a much smaller load (about 90kN) than the first since the tensile strength of the concrete could no longer be utilised. Once a minimal state of tension was attained in the bottom fibre of the beam the former cracks reopened and curvature of the load-deflection plot immediately resulted. The upper portion of the load-deflection curve for this second cycle fits an extrapolation of the curve for the first cycle, and the overall shape represents the response for monotonic loading. Loading in this cycle continued up to 153 kN and at this stage many flexural cracks were evident, all at a fairly constant spacing. Upon load removal a sharp noise was heard from one end of

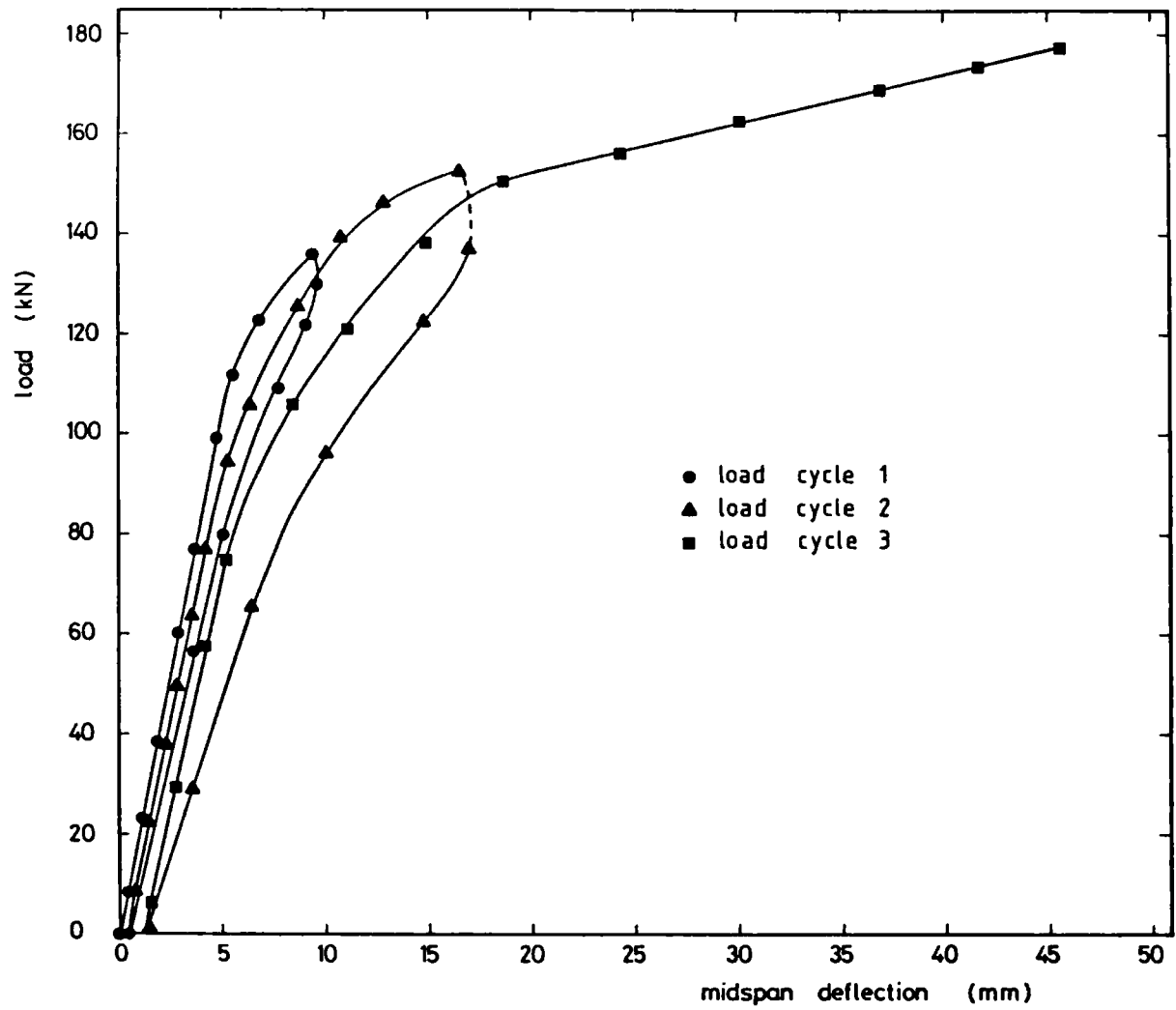


Figure 9.15 Ultimate test load-deflection curve.

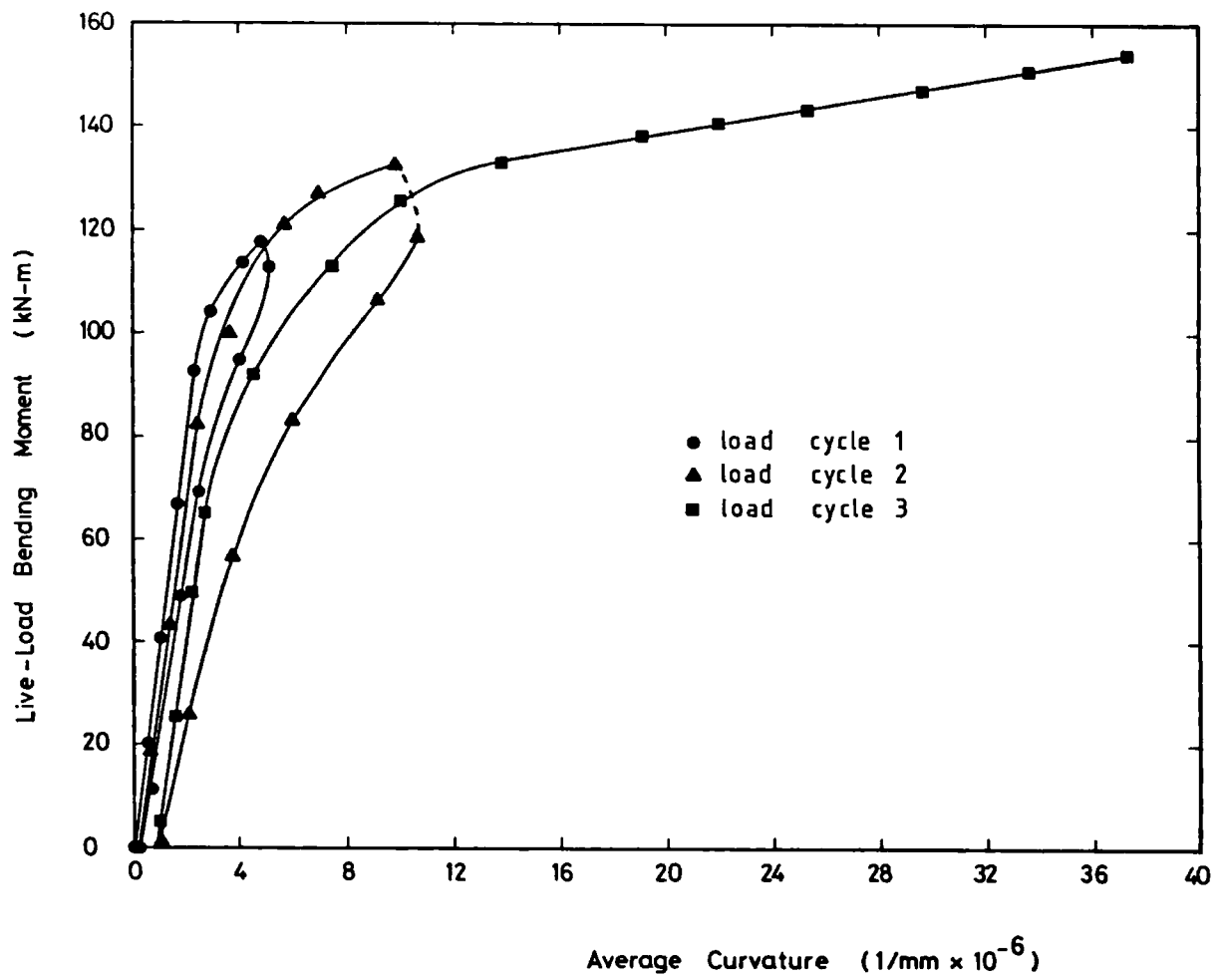


Figure 9.16 Ultimate test moment-curvature diagram.

the beam and a small step indicating a slight load reduction was observed in the X-Y plot.

This step (shown as a broken line in Figures 9.15 and 9.16) was most likely caused by a slight slip of the rope and steel spike within an end termination. Slip of this nature, as opposed to the gradual bed-down that takes place during pretensioning, tends to occur when the rope is subjected to loads that it has not experienced before; a mechanism for this slip, which commonly occurred during the rope tension tests, is described in 4.5.3. An analysis of the test results revealed that beam curvature, and friction developed between the outer sheath and the prestressing duct, had increased the load in the rope to about 460 kN, some 45 kN (4.5 tonnes) above the initial prestress. Such an increase had not been expected, particularly as the rope had been wrapped in PTFE tape, but having occurred it was probable that some rope slip would result. Slip of this nature could not, of course, occur under normal working load conditions. Furthermore, the procedure of pretensioning the rope beyond a level to which it would later be subjected would prevent such occurrences.

The third and last cycle of loading once again revealed an initial linear load-deflection curve (Figure 9.15). The curve became non-linear at a slightly smaller load than cycle 2, however, reflecting the small loss of prestress due to anchorage slip. Consequently, the extrapolation of the previous load-deflection curve was not quite regained.

As the loading was increased beyond previous levels existing cracks began to extend into the web and widen; additional cracks also developed at regular spacing. Large increases of beam deflection were observed for only small load increases as the beam entered what may be described as a cracked elastic state (Figure 9.13), so-called since any permanent deformation would probably be small and any cracks would close completely upon load removal. As the load was further increased cracks began to extend to the top of the web (see Figure 9.17) indicating a very high neutral axis and that beam failure was imminent. Finally an explosive, brittle failure occurred as a limiting compressive strain was reached in the top flange; considerable crushing of the concrete resulted with large pieces of the top flange becoming detached as shown in Figure 9.18. As failure

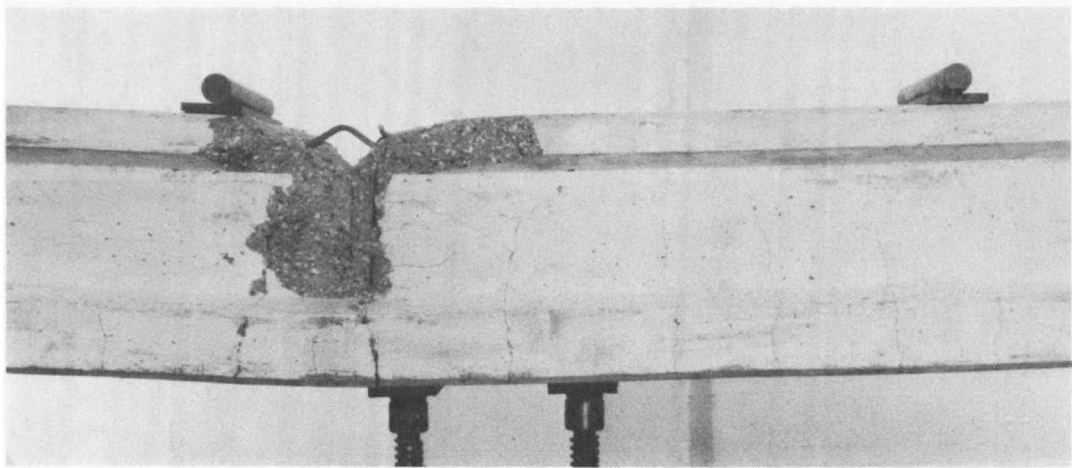


Figure 9.17 Beam after failure showing distribution of flexural cracks between loading platens.

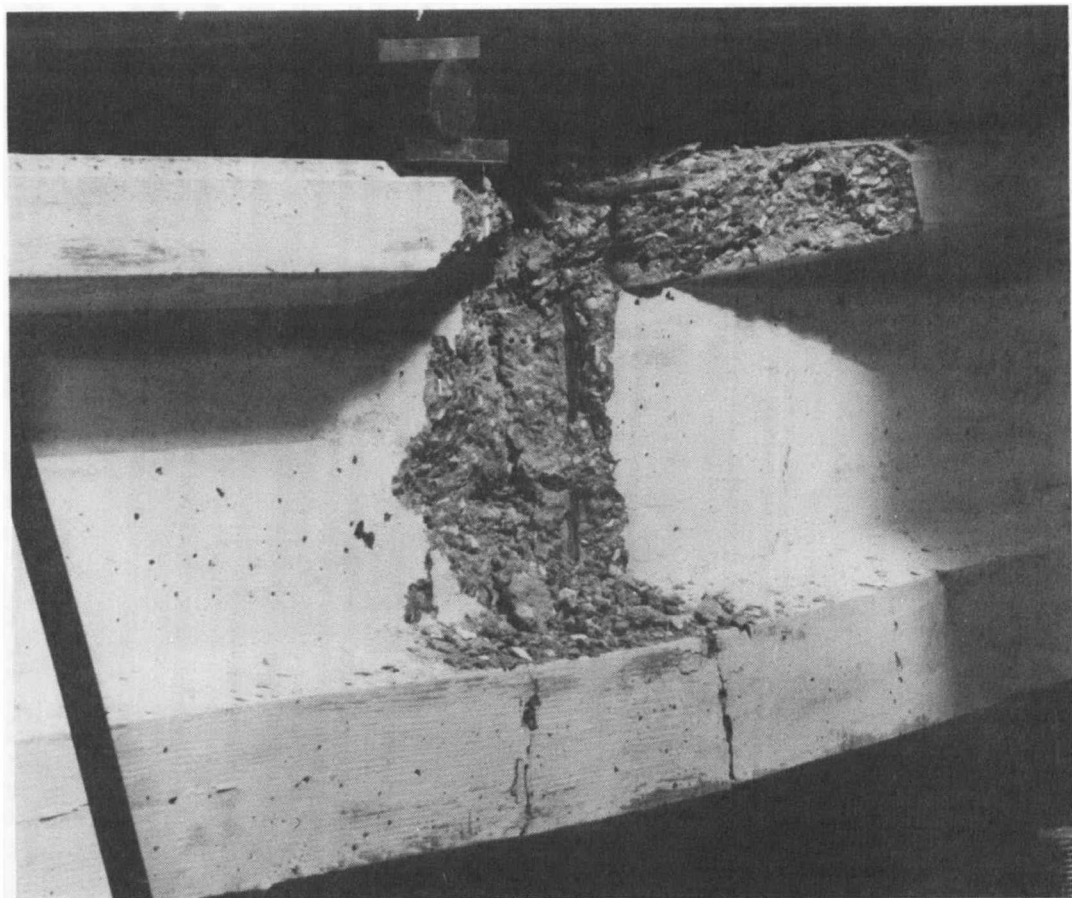


Figure 9.18 Typical compressive failure with extensive crushing.

occurred, compressive stresses in the top flange were shed into the web, which failed in its turn. There was even some crushing of the top of the bottom flange.

This type of failure is typical for beams containing unbonded tendons. Since the Parafil was free to elongate over its entire length and, therefore, did not undergo high additional local straining, it did not reach its ultimate breaking load before the concrete failed in compression. Test data for the ultimate test are shown in Table 9.4

Ultimate load	177.4 kN (18.1 tonnes)
Ultimate live bending moment	154.0 kN-m
Dead load bending moment	4.1 kN-m
Ultimate moment of resistance	158.1 kN-m
Approx. contribution from untensioned steel	25 kN-m (16%)
Midspan deflection at ultimate load	45.4 mm
Average curvature at ultimate load	$36.7 \times 10^{-6} \text{ mm}^{-1}$
Approx. prestress at failure	507 kN (30% increase)
Bond factor at failure	0.24

} see Appendix 8

Table 9.4 Ultimate test data.

9.8 The determination of the prestress force and the degree of bond during the ultimate test

The observed ultimate bending moment (158.1 kN-m) was 19% higher than the initial design prediction (132.4 kN-m) and this was mainly due to two reasons. Firstly, match-cured cube specimens gave an average compressive strength of 57.5 MPa compared with 40 MPa assumed at the design stage, due primarily to a delay in the testing schedule. Secondly (and more importantly) an analysis of the test readings revealed that the stress in the rope under ultimate conditions had increased considerably more than the 10% which had been assumed at the

design stage. The increase in rope stress can be attributed to friction developed between the rope and the prestressing duct, and beam curvature.

A method for determining the stress in the rope throughout the final load cycle is described in Appendix 8. The method is based on the strain measured in the untensioned longitudinal steel and an assumed stress-strain curve for concrete as developed by Desayi and Krishnan (1964). It is estimated using this technique that the stress in the rope just prior to beam failure was 30% greater than under zero-load conditions, hence the significant increase in ultimate bending moment.

The increase in strain in a tendon under ultimate conditions is typically represented by the expression $\beta_2 \epsilon_u$ where β_2 is called the equivalent bond factor and ϵ_u is the concrete strain at tendon level. For bonded tendons, as would be expected, β_2 assumes a value of unity, but for unbonded tendons β_2 is much less. The equivalent bond factor consistent with the stress increase observed in the Parafil tendon was found to be 0.24 (see Appendix 8). It should be remembered, however, that this bond factor is only applicable to these particular test conditions and to the prestressing materials used (i.e. for a polyethylene outer sheath wrapped in PTFE tape and surrounded by a plastic prestressing duct).

9.9 Discussion

The behaviour of a beam, prestressed with a Parafil tendon has been shown to be acceptable and there appears to be no departure from existing theory in predicting beam behaviour.

The first test (9.6) proved the adequacy of the prestress and indicated that the beam remains linearly elastic when subjected to maximum working loads. The second test (9.7) resulted in an ultimate moment of resistance greater than initially anticipated, but in line with that expected given the stronger concrete and the greater than predicted increase in tendon stress. Clearly more work needs to be done to establish the contribution to the increase in tendon stress due to friction so that ultimate moments of resistance can be assessed more accurately. An equivalent bond factor of around 0.24 would seem

to be appropriate here (Appendix 8) if the ultimate moment of resistance were to be calculated using normal methods. It should be noted, however, that the outer sheath of the rope was wrapped in PTFE tape and without this the increase in rope stress would probably have been higher.

The mode of beam failure, characterised by crushing of the top flange, was typical for a concrete beam prestressed with an unbonded tendon. Even so, the beam exhibited a considerable degree of ductility prior to failure due to cracking in the tension zone of the concrete. The inclusion of a small amount of untensioned longitudinal reinforcement (which accounted for only 16% of the beam's ultimate moment of resistance) contributed to the ductility by ensuring a fairly uniform distribution of cracks. Hence, despite Parafil and concrete exhibiting brittle modes of failure, a ductile composite of the two materials can be made.

The test demonstrated clearly the engineering simplicity of the Parafil prestressing system and the consequent ease with which Parafil beams can be prestressed (9.4). Also, the concrete end-blocks, designed using traditional techniques, were found to behave well throughout the tests; there was no evidence of cracking in the end zones upon completion of testing.

Finally, the Parafil rope itself behaved durably, retaining a residual load of 34 tonnes (the load found to remain in the rope after beam failure) quite satisfactorily for several days until the beam was destressed. Hence, the test has acted as a clear demonstration to engineers that Parafil can be employed successfully and without difficulty as a tendon in prestressed concrete.

CHAPTER 10: EPILOGUE

10.1 Conclusions

Parafil Type G ropes, produced by ICI Linear Composites Ltd., consist of a closely packed core of Kevlar 49 yarns encased in a durable polymeric sheath. The structural application of Parafil Type G ropes as tendons in prestressed concrete has been investigated and shown to be both feasible and practicable.

A literature survey of the properties of Kevlar yarns and fibres (Chapter 2) revealed that they exhibit excellent tensile properties, low creep characteristics, good tensile fatigue performance and good chemical resistance. It was not surprising to discover, therefore, that Kevlar had already found wide applicability. Given these attractive qualities it was inevitable that consideration would eventually be given to the use of ropes made from Kevlar in the field of prestressing. Funding for such research was provided jointly by ICI Linear Composites Ltd. and the Science and Engineering Research Council.

Ropes made from Kevlar, of various forms of construction, have been in production for many years. It was shown (Chapter 3) that the good tensile and creep properties of Kevlar yarns and fibres were not reflected well in Kevlar ropes of braided or twisted construction and hence that these ropes were unsatisfactory candidates for prestressing tendons. Initial tests (by other investigators) on the other hand, indicated that parallel-lay ropes exhibited properties similar to Kevlar itself; of the three basic forms of construction, parallel-lay ropes were found to display the highest strength, the highest stiffness and the lowest creep. It was argued (3.5) that Parafil Type G ropes, therefore, were likely to be the most suitable Kevlar ropes for use as prestressing tendons.

An investigation into the properties of Parafil Type G ropes, relevant to the field of prestressing, was thus undertaken. A rig for testing 60 tonne ropes was designed and fabricated and a prestressing system that enabled Parafil ropes to be stressed and anchored was developed. Tension tests on 60 tonne ropes (Chapter 4) revealed a broadly linear

stress-strain curve and a brittle mode of failure. The mean breaking load of the ropes (60.9 tonnes) was marginally higher than that claimed by the manufacturer but the elastic modulus (118 GPa) was slightly less.

Prestressing tendons are permanently highly stressed and so considerable attention was devoted to the time-dependent properties of Parafil. The stress-relaxation of 60 tonne Parafil Type G ropes was studied over a broad stress range (30% to 70% NBL) with testing times lasting up to 1000 hours (Chapter 5). In agreement with some work done by other researchers on Kevlar, stress-relaxation in Parafil Type G ropes was found to show only a marginal dependence on the applied stress. A linear relationship between stress-relaxation and the logarithm of time was observed and on this basis an expression for estimating long-term stress-relaxation (as would be required by designers) was determined. It was predicted from this expression that the relaxation after 100 years for a rope stressed initially to 50% NBL would be 7.8% NBL. Tension tests were conducted on the stress-relaxation rope specimens and these indicated (on average) 98% strength retention. Despite the higher loss of prestress due to stress-relaxation, when compared to steel, total expected prestress losses in Parafil Type G ropes were found to compare favourably with steel tendons.

It was recognised at an early stage that the stress-rupture phenomenon displayed by Kevlar would influence the operating stresses adopted for Parafil tendons. To investigate this most important property a series of stress-rupture tests on 60 tonne ropes was performed (Chapter 6); the tests were conducted at high stress levels so that the failure times would be sensible. A linear trend between the applied stress and the logarithm of the 'time to break' was observed; an extrapolation of the 'best-fit' line representing the lifetime data led to the prediction that if a Parafil Type G rope were subjected to a load of 50% of its nominal breaking load it would fail in stress-rupture after 100 years. An alternative (and probably optimistic) empirical method for making stress-rupture lifetime predictions, based on measured creep strain data, was postulated. The predictions at low stresses were highly dependent on the assumed correlation between creep rate and stress; this correlation remains to be confirmed (or rejected) through further testing but it should be recognised that

there is strong theoretical and experimental evidence to suggest that the linear trend between stress and log. lifetime will persist even at low stress levels.

A review of the theoretical and experimental approaches to stress-rupture in polymers was conducted (Chapter 7) with the objective of establishing a basis upon which more confident stress-rupture predictions for Parafil could be made and hence of determining safe operating stresses for Parafil tendons. A thorough statistical model based on experimental stress-rupture data for Kevlar 49 impregnated yarns was favoured since it enabled a rational, probabilistic approach to design to be adopted. Good agreement between this model and the stress-rupture data for Parafil was obtained when the results were rationalised with respect to mean breaking strength. A cumulative damage technique was developed so as to take account of the loading history of a Parafil tendon and thus maximise the initial prestress that can be applied. Safe operating stresses (as a continuous function of the chosen structural design life) based on a 1 in 10^6 probability of failure were determined for three separate loading histories; allowable values for the initial prestress were found to range from 47% NBL (assuming minimum prestress losses and a design life of 100 years) to 56% NBL (assuming maximum prestress losses and a design life of 20 years). Environmental effects on operating stresses were considered.

The theoretical distribution of tensile strength of 6 tonne Parafil Type G ropes was determined from the experimental strength distribution of individual Kevlar 49 yarns (Chapter 8). A test rig was designed and fabricated for testing this size of rope and tests were conducted to determine the experimental rope strength distribution. The actual distribution was found to be Gaussian as predicted by the theory and excellent agreement was obtained between the experimental and theoretical variability of rope strength; this was encouraging since it implied that the stress-rupture predictions for Parafil tendons, based on Kevlar 49 yarn lifetime data, were likely to be conservative. However, the experimental mean rope strength was lower than that predicted theoretically; possible causes for the discrepancy were investigated but the issue was not entirely resolved. Tests were conducted to study the effect of rope length on strength, batch effects, and the effect of stretching ropes around a point of

deflection. No statistically significant reductions in strength were observed in either the 'length' or the 'deflector' tests; however, a marginal increase in strength was observed when a sample of ropes from a different batch was tested.

Finally, as a demonstration of Parafil's ability to provide the prestress to a concrete member, a full-scale beam test was performed (Chapter 9). The beam, of 5 metres length, contained a single, straight, unbonded 60 tonne tendon stressed initially to 70% of its nominal breaking load (i.e 42 tonnes). Two tests were performed on the beam. The first test proved the adequacy of the beam and the applied prestress to cope with typical working loads. In the second test the beam was loaded through to failure; the mode of failure, characterised by crushing of the compression flange, was typical for a concrete beam prestressed with an unbonded tendon. The ultimate moment of resistance of the beam was higher than expected due to the concrete strength and the increase in rope stress (under ultimate conditions) being higher than anticipated. The tests provided a clear demonstration for engineers that Parafil Type G ropes can be employed successfully and with ease as tendons in prestressed concrete.

10.2 Interim guidance for engineers

Sufficient experience and confidence in Parafil as a structural material has now been developed to enable interim guidelines to be presented to engineers covering the use of Parafil Type G ropes as tendons in prestressed concrete. The guidelines which follow consist of a statement or recommendation followed by some explanation or discussion if necessary.

(1) PARAFIL TYPE G ROPES SHOULD ONLY BE USED AS UNBONDED TENDONS IN PRESTRESSED CONCRETE.

Parafil ropes as presently made do not bond to concrete although possible techniques for developing bond could obviously be postulated. The lack of bond is advantageous, however, as it will limit the additional strain in the Parafil tendon when prestressed concrete members are subjected to overload conditions; this is important given Parafil's brittle mode of failure.

(2) THE INITIAL STRESS IN A PARAFIL TYPE G TENDON SHOULD BE RESTRICTED TO THE VALUES SHOWN IN FIGURE 10.1.

Figure 10.1 is a reproduction of Figure 7.19. A knowledge of the structural design life and the magnitude of the time-dependent prestress losses is required (as discussed in Chapter 7) if the initial prestress in Parafil tendons is to be maximised.

(3) THE INITIAL PRESTRESS MAY BE INCREASED ABOVE THE VALUES SHOWN IN FIGURE 10.1 TO ALLOW FOR THE PRESTRESS LOSS DUE TO ELASTIC DEFORMATION OF THE CONCRETE ONLY IF CONTINUITY OF THE PRESTRESSING OPERATION CAN BE GUARANTEED.

It is probably unwise to overstress in this manner; if problems were to develop during the stressing operation then the tendons which had already been stressed and anchored could accumulate stress damage greater than anticipated thus threatening the future integrity of the member (7.9.5).

(4) THE TENDON STRESS AT THE ANCHORAGE POINT SHOULD NOT BE INCREASED TO COMPENSATE FOR FRICTIONAL LOSSES AT CRITICAL SECTIONS OF THE MEMBER.

If a compensating overstress were applied the probability of failure of the tendon at the anchorage point (where the frictional losses are zero) would increase beyond 1 in 10^6 (7.9.6).

(5) ACCOUNT SHOULD BE TAKEN OF THE FLUCTUATION OF PRESTRESS DUE TO TEMPERATURE CHANGES.

. Research into the effects of stress and cycles of temperature on the longitudinal expansion of Kevlar 49 is required. For the time being, however, it must be assumed that Kevlar 49 exhibits a slightly negative coefficient of longitudinal expansion under all conditions. On this basis it is recommended that the temperature at which a Parafil tendon is stressed on site (in the British Isles) be limited to the range $+4^{\circ}\text{C}$ to $+23^{\circ}\text{C}$ in order to restrict the prestress fluctuation to between $+3\%$ NBL and -4% NBL respectively. The effect of this fluctuation on concrete stresses should be considered. Also, for a tendon stressed at the proposed minimum permissible temperature

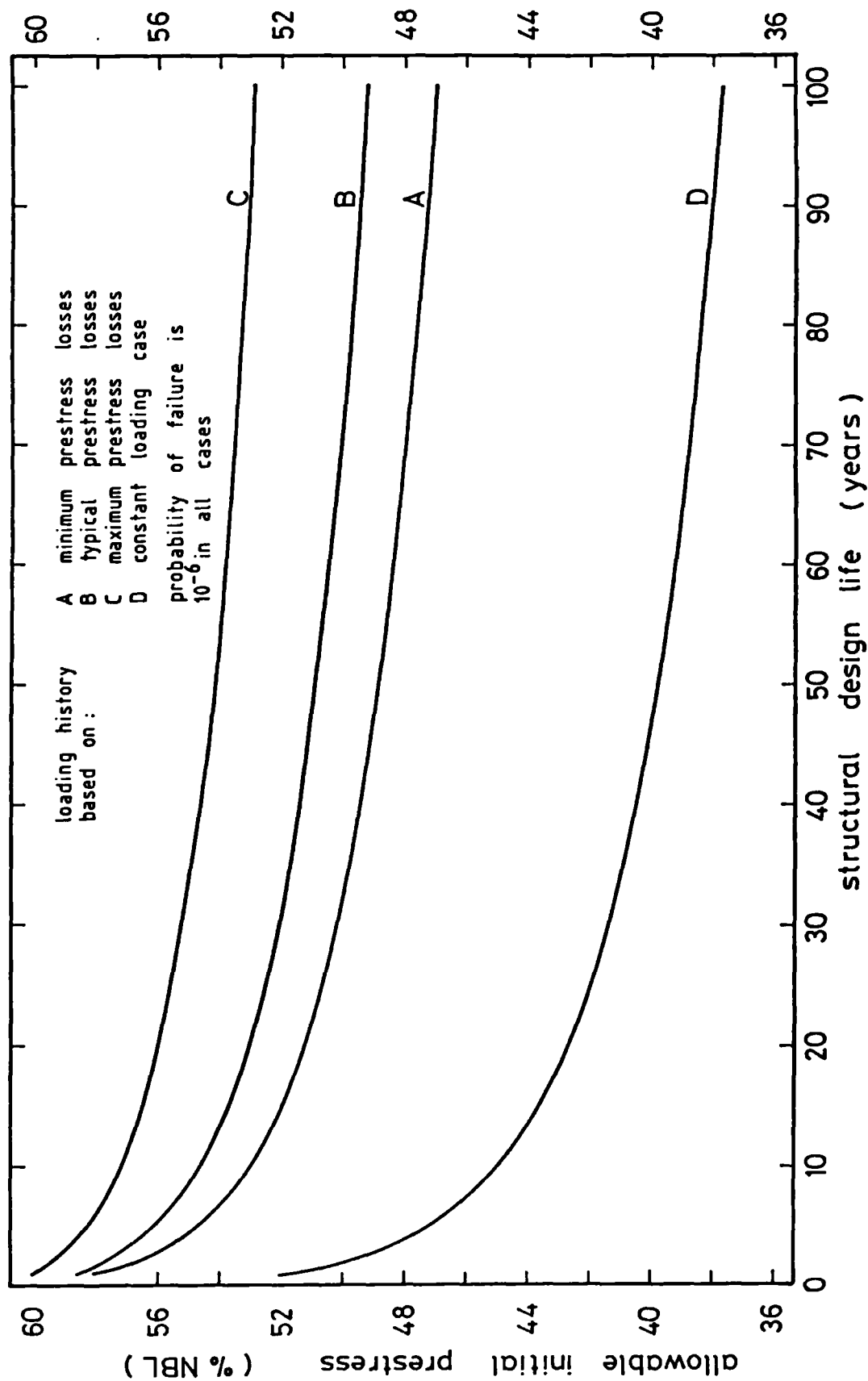


Figure 10.1 Plot of allowable initial prestress against structural design life for various loading histories.

of + 4°C it is recommended that the initial prestress (as specified by Figure 10.1) be reduced by 1% NBL to allow for the averaged yearly stress increase due to temperature increases (7.13).

(6) FOR AREAS OF THE WORLD WHERE THE MEAN AIR TEMPERATURE IS GREATER THAN 28°C CONSIDERATION SHOULD BE GIVEN TO REDUCING THE OPERATING STRESSES IN PARAFIL TENDONS BELOW THOSE SHOWN IN FIGURE 10.1.

The operating stresses for Parafil were partly based on the results of tests conducted at 28°C on Kevlar 49 pressure vessels. Research by other investigators has shown that stress-rupture lifetimes for Kevlar 49 decrease as the temperature increases (see Figure 7.6 for example).

(7) THE LOSS OF PRESTRESS DUE TO STRESS-RELAXATION IN PARAFIL TENDONS SHOULD BE CALCULATED USING EQUATION 10.1.

$$r = 1.82 + (4.03 \times 10^{-2} \times f) + 0.67 \log_{10} t \quad 10.1$$

where: r is the stress-relaxation as a % NBL

f is the initial stress as a % NBL

t is the structural design life in hours.

(Equation 10.1 is an approximation to Equation 5.2)

(8) LOSSES OF PRESTRESS DUE TO CONCRETE CREEP, SHRINKAGE AND ELASTIC DEFORMATION SHOULD BE CALCULATED IN THE NORMAL MANNER ASSUMING AN ELASTIC MODULUS FOR PARAFIL TYPE G OF 118 GPA.

(9) PARAFIL TERMINALS SHOULD BE FITTED CAREFULLY IN ACCORDANCE WITH THE MANUFACTURER'S INSTRUCTIONS AND PREFERABLY UNDER 'FACTORY' CONDITIONS.

(10) ALL ROPES SHOULD BE PRETENSIONED TO AT LEAST 60% NBL IN THE MANUFACTURER'S PRESCRIBED MANNER.

(11) IN THE UNLIKELY EVENT THAT A ROPE IS TO BE SUBJECTED TO STRESSES GREATER THAN 60% NBL UNDER NORMAL WORKING LOAD CONDITIONS THEN CONSIDERATION SHOULD BE GIVEN TO PRETENSIONING THE ROPE BEYOND 60% NBL.

In this way stress losses due to bed-down can be eliminated (5.5) and

the possibility of sudden spike 'jumps' (9.7) can be precluded.

(12) THE LENGTH OF PARAFIL ROPES SHOULD BE CAREFULLY DETERMINED.

This is necessary since Parafil terminals must be fitted before incorporation in the concrete member and before any stress is applied. Assuming that the concrete member is to be used as the reaction frame for the pretensioning process allowance should be made for:

- (a) rope bed-down during pretensioning (4.5.2)
- (b) the disorientation effect (eg. see Table 4.1)
- (c) rope bed-down during the prestressing operation (eg. see Figure 4.10)
- (d) rope stretch during the prestressing operation (based on $E = 118 \text{ GPa}$).

The outer thread on the terminals (4.3) will provide a fair degree of tolerance in the rope length estimation.

(13) CONSIDERATION SHOULD BE GIVEN TO PROVIDING SOME UNTENSIONED LONGITUDINAL REINFORCEMENT TO ENSURE BETTER CRACK DISTRIBUTION AND ENHANCED DUCTILITY UNDER OVERLOAD CONDITIONS.

Some longitudinal steel will be needed even if only to enable the shear links to be fixed. The detailing requirements associated with crack control in members with unbonded steel tendons should be followed.

(14) NO ALLOWANCE NEEDS TO BE MADE FOR PROVIDING CONCRETE COVER TO PARAFIL TYPE G TENDONS.

Parafil has very good chemical resistance and does not corrode like steel tendons. Tests have shown, however, that Kevlar does lose strength in some strongly alkaline solutions (Table 2.2); concrete is also highly alkaline but intimate contact between Parafil and concrete is prevented by the chemically inert thermoplastic outer sheath.

10.3 Recommendations for further research

There is probably sufficient information contained within this thesis

to enable engineers to safely design and construct concrete members incorporating Parafil Type G prestressing tendons. Nevertheless, inevitably there are areas where more information, furnished by further research, could provide an added injection of confidence or perhaps enable a more refined approach to be adopted.

This research has identified the important need to conduct longer-term stress-rupture tests. With this objective in mind, as part of an ongoing research programme at Imperial College, a series of loading frames for conducting dead-load stress-rupture tests on small Parafil Type G ropes has recently been commissioned (Guimares and Burgoyne 1986). It is intended that the tests will last years rather than months; hopefully, the results of the test programme will confirm the trends established from this research and thus inspire even greater confidence in the long-term stress-rupture predictions.

While these long-term tests are in progress it is recommended that tests be conducted to measure the rate of creep of Parafil ropes when subjected to low stress levels (say 10% NBL to 50% NBL). The duration of the tests would only need to be sufficient to establish the constant rate of creep with the logarithm of time. The results would (to a large extent) enable the empirical method for predicting long-term stress-rupture lifetimes based on strain criteria (Chapter 6) to be validated (or otherwise).

As part of the process of deriving safe operating stresses for Parafil Type G tendons (Chapter 7) it was necessary to define the loading history for the tendon. The loading history (as defined) was based partially on the results of the constant strain stress-relaxation tests conducted earlier in the programme (Chapter 5). In practice, however, the length of prestressed concrete members reduces with time due to shrinkage and creep of the concrete and so the loading history definitions could only be approximated. It is not recommended that tests to measure the decay of stress in a Parafil tendon when subjected to a decreasing strain be conducted owing to the practical difficulties of performing such tests. However, if a series of creep tests were conducted at low stress levels, then it may be possible to relate the results of the creep tests to the stress-relaxation tests (conducted as part of this research) using a suitable rheological model. As discussed earlier (5.9) the formulation of a realistic

model is likely to be problematical but if it were determinable then the decay of stress in a Parafil tendon when incorporated in a concrete member could be evaluated more accurately. This would enable the loading history definitions to be refined but it is thought that the effect on the safe operating stresses (Chapter 7) would not be great. Similarly, the effect on the estimation of total prestress losses in Parafil tendons would probably be small. This is not to say that the exercise would not be worthwhile, however, since rheological models are useful tools for investigating a material's response to all types of loading.

Research is needed into the effects of the environment on Parafil ropes when subject to stress. For instance, in the discussion on the fluctuation of prestress brought about by temperature changes (7.13) it was assumed that the coefficient of linear expansion of Parafil Type G tendons when subjected to stress was the same as for non-stressed Kevlar fibres (i.e. $-2 \times 10^{-6}/^{\circ}\text{C}$). Guimares (1985) as part of the ongoing research programme into Parafil Type G tendons has begun to investigate the effect of stress on the coefficient of linear expansion of Kevlar 49 yarns at various temperatures. The results should enable prestress fluctuations in Parafil tendons and their effects on concrete stresses to be evaluated more accurately. Also, it would be useful to determine the effect of relative humidity (if any) on the stress-rupture lifetimes of Parafil Type G tendons since the results of existing work on Kevlar 49 appear to conflict as discussed in 7.14.

It was not possible to fully explain the significant discrepancy between the theoretical and experimental tensile strength efficiencies of 6 tonne Parafil Type G ropes (8.10). Since little experimental work has been done to validate the statistical bundle theory employed it is possible that it contains deficiencies; this is one area where further research may be needed but any proposed change in the theory would need to be validated by testing bundles of fibres (or yarns) of materials other than Kevlar 49; the problems of manufacturing and of determining suitable methods for gripping and stressing such samples are likely to be difficult to resolve. On the other hand, it is also possible that the deficiency in strength could be bridged by small improvements in the manufacturing process. It was not possible to inspect the manufacturing process during this research programme but

this ought to be reconsidered in the future given the possible economic benefits.

Finally, it is important to be able to accurately predict the ultimate strength of members incorporating Parafil Type G tendons. As discussed earlier (Chapter 9) the ultimate strength is partially dependent on the additional strain induced in the tendon under overload conditions. The additional strain, which is commonly expressed in terms of an effective bond factor, is attributable to an increase in the distance between the anchorage points and also frictional effects. Research is recommended to determine suitable bond factors for Parafil; the research would be important not only so that full account could be taken of the ultimate strength of Parafil members in design but since excessive additional rope straining could lead to brittle failure.

Appendix 1 Manufacturers of Kevlar ropes and some basic properties

ICI Linear Composites Ltd
 Hookstone Road
 Harrogate
 North Yorkshire
 England
 HG2 8QN

Basic Characteristics of Types F and F/C, G and G/C Parafil Ropes (Note Type G ropes have a higher elastic modulus than Type F ropes)

Nominal diam (mm)	Nominal breaking load in standard and connectors (tonnes)	Nominal diam of fibre core (mm)	Cross sectional area of fibre in core (mm ²)	Approx wt in air (kg/100M)	Estimated wt* in seawater core flooded (kg/100M)
7	1.5	4	7.64	3.7	0.25
8.5	3	5.4	15.28	5.4	0.36
11	6	7.6	30.55	9.1	0.86
13.5	10.5	10	53.47	14.9	2.8
17	15	12.5	76.38	21.5	3
20	22.5	15	114.6	30	4
22	30	17	152.8	37.5	6
31	60	22	305.5	72	7
36	90	29	458.3	100	16
45	150	38	763.8	159	34

NOTE: * Since PARAFIL ropes consist of a closely packed core of cylindrical filaments there is always an air space amounting to 25-30% of the cross sectional area of the core. If the ropes are sealed to prevent the penetration of water then they will float. If the ropes are allowed to become completely saturated they will have this weight in seawater.

Tensile Stress and Elastic Modulus

Rope	Ultimate Tensile Stress	Elastic Modulus (Youngs)	
Type A	6,286 kg/cm ² 0.6166 kN/mm ²	123,253 kg/cm ² 12 kN/mm ²	Type A: Terylene (polyester)
Type F	19,633 kg/cm ² 1.926 kN/mm ²	792,806 kg/cm ² 77.7 kN/mm ²	Type F: Kevlar 29
Type G	19,633 kg/cm ² 1.926 kN/mm ²	1,290,000 kg/cm ² 126.5 kN/mm ²	Type G: Kevlar 49

NOTE: All 'A' ropes, eg Type A, Type A/C and Type A/Pu have the same core and therefore the same tensile properties. This is also true for the 'F' and 'G' series of ropes.

A Parafil rope consists of a closely packed core of high strength continuous synthetic yarns encased in a durable polymeric sheath. Ropes with breaking loads ranging from 0.2 to 1500 tonnes are manufactured. A variety of sheathing materials can be provided.

Cordes Europe France
 87 Rue de la Fraternite
 93106 Montreuil Cedex
 France

CEFOR® : STANDARD ROPES

Physical Dimensional and Mecanical Characteristics

Reference	Construction	Physical and Dimensional Characteristics						Mecanical characteristics				
(Type) CEFOR ROPE of :	Specifications	Number of layers	BARE ROPES		SHEATHED ROPES			Breaking Load		Elongation to break %	Minimum bending radius	
			Weight gr/metre	Diameter m/m	Weight gr/metre	Sheat thickness m/m	Diameter m/m	Kgs	Lbs		Dynamic m/m	Static m/m
0.8 Tonnes	3x1.5 / TW A3	1	7	3	13.5	0.4	4.1	840	1,850	3.5	80	40
1.1 Tonnes	4x1.5 / TW A3	1	9	3.4	16.2	0.4	4.3	1,120	2,470	3.5	90	45
2 Tonnes	7x1.5 / TW A3	1	15	4.7	24	0.4	5.5	2,000	4,415	3.5	110	55
3 Tonnes	7x2 / TW A3	1	26	6	37	0.5	7	3,400	7,505	3.5	140	70
5 Tonnes	19x1.5 / TW A3	2	40	7.6	63	0.6	8.8	5,100	11,260	3.5	180	90
8 Tonnes	19x2 / TW A3	2	69	10	93	0.6	11.2	8,200	18,000	3.5	230	115
10 Tonnes	37x1.5 / TW A3	3	78	10.6	112	0.7	12	10,000	22,075	3.5	240	120
17 Tonnes	37x2 / TW A3	3	134	14.4	203	0.8	16	17,200	37,970	3.5	320	160
25 Tonnes	61x2 / TW A3	4	220	19	300	1	21	25,000	55,190	3.5	420	210

Cordes Europe France make ropes of twisted contruction using either Kevlar 29 or Kevlar 49 strands. Ropes may be produced with or without a sheath. The designation 19 x 2/TWA3 refers to 19 Kevlar 29 strands of 2 mm diameter.

Vermeire N.V.
Kapellestraat 44
B-9160 Hamme
Belgium

Caver cables based on Kevlar 29 are produced by Vermeire. They have recently been used on the Eastern Scheldt Storm Barrier to help secure construction vessels. A total length of 6 kilometres of cable was used of diameters 51 mm and 96 mm. The nominal breaking strength of the 96 mm cable was 387 tonnes. This cable, with a twisted construction, consisted of 8 outer and 6 inner strands of 15000 denier rovings.

Wall Ropes Works
 Yale Cordage Inc
 Old Sparhawk Mill
 Yarmouth
 Maine 04096
 USA

MINILINE® SPECIFICATIONS

Diameter			Breaking Strength (Average Lbs.)	Weight In Lbs./100'
Inches	Decimal	M.M.		
1/16	.0625	1.59	450	.15
3/32	.0938	2.38	850	.31
1/8	.125	3.18	1,650	.61
5/32	.1563	3.97	2,500	.92
3/16	.1875	4.76	3,500	1.2
1/4	.25	6.4	5,700	2.1
5/16	.3125	7.9	8,300	3.3
3/8	.375	9.5	12,300	4.7
7/16	.4375	11.1	16,700	6.3
1/2	.5	12.7	22,000	8.4
9/16	.5625	14.3	27,500	10.6
5/8	.625	15.9	33,000	13.1
3/4	.75	19.1	46,500	18.9
7/8	.875	22.2	60,000	25.9
1	1.0	25.4	78,000	33.8

Miniline is a braided rope with a tightly braided abrasion resistant polyester jacket. A low braid angle is used in the construction to reduce internal compressive forces when under load. Ropes up to 35 tonnes breaking strength are available in either Kevlar 29 or Kevlar 49.

Whitehill Manufacturing Corporation
 P O Box 356
 Lima
 Pennsylvania 19037
 USA

JETSTRAN III

The following data describes six strand double jacketed "Kevlar 29"* ropes. These are general purpose ropes and should be considered where flexibility, strength to weight, ease of handling and ease of termination are important.

<u>Outside diameter (inches)</u>	<u>Break strength** (pounds)</u>	<u>Weight in air*** (lbs/100')</u>
1/2	15,000	8
5/8	20,000	11
3/4	30,000	16
7/8	40,000	22
1	50,000	27
1-1/8	65,000	35
1-1/4	80,000	43
1-1/2	100,000	54
1-5/8	120,000	65
1-3/4	140,000	75
1-7/8	160,000	86

Whitehall Manufacturing Corporation market four separate ranges of ropes for a variety of applications. They all incorporate Kevlar 29 and are based on the twisted form of construction. Ropes with breaking strengths ranging from 1.3 tonnes to 200 tonnes are manufactured. A range of general purpose ropes is shown above.

Philadelphia Resins Corporation
 20 Commerce Drive
 Montgomeryville PA
 USA

PHILLYSTRAN® WR

Item Number	Diameter (Including Jacket)		Break Strength		Approximate Weight (Including Jacket)*		Standard Reel Length
	inch	mm	lb	kg	lb/1000 ft	kg/km	ft
PSWR-005	1/8	3.2	1000	454	4	6.0	5000
PSWR-008	3/16	4.8	3000	1361	9	13.4	5000
PSWR-010	1/4	6.4	6000	2722	23	34.2	5000
PSWR-020	3/8	9.5	12,000	5443	42	62.5	5000
PSWR-030	7/16	11.1	18,000	8165	61	90.8	4000
PSWR-040	1/2	12.7	22,000	9979	78	116.1	3500
PSWR-050	5/8	15.9	33,000	14,969	114	169.6	2000
PSWR-060	3/4	19.1	50,000	22,680	167	248.5	1500
PSWR-070	7/8	22.2	62,000	28,123	220	327.4	1000
PSWR-080	1	25.4	83,000	37,649	288	428.5	700
PSWR-090	1 1/8	28.6	105,000	47,628	376	559.5	500

LARGE DIAMETER 6 x 7

Part Number	Nominal Diameter Bare Rope	Calculated Break Strength	Calculated Weight of Bare Rope	Maximum Length
PS29-6x7x1.2	1.2 in	75,000 lb	0.35 lb/ft	900 ft
PS29-6x7x1.5	1.5 in	105,000 lb	0.57 lb/ft	550 ft
PS29-6x7x1.7	1.7 in	144,000 lb	0.70 lb/ft	425 ft
PS29-6x7x2.1	2.1 in	202,000 lb	1.00 lb/ft	245 ft
PS29-6x7x2.5	2.5 in	272,000 lb	1.34 lb/ft	180 ft

The 6x7 large diameter ropes are composed of six strands of PHILLYSTRAN around a rope core.

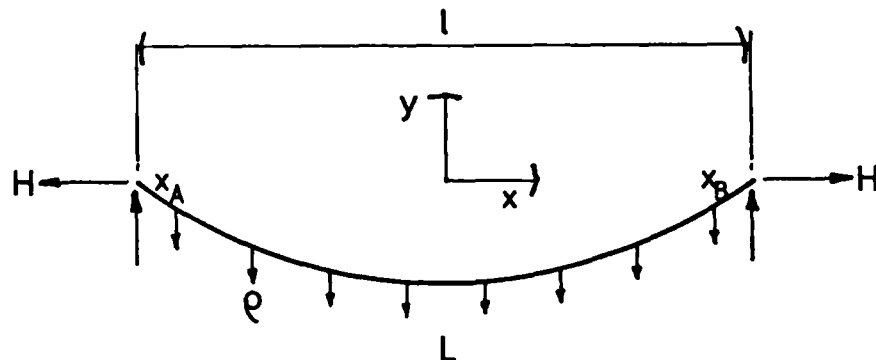
Philadelphia Resins Corporation manufactures both twisted and braided ropes. The ropes may be jacketed with either a standard polyurethane extruded sheath or a polyester braided jacket. From the details of twisted ropes shown above it can be seen that ropes with breaking strengths up to 121 tonnes are manufactured.

Appendix 2: Rope catenary effects on load-displacement curves for 60 tonne Type G Parafil ropes

For a cantenary (cable loaded by its own weight) it can be shown that (e.g. Brown 1976):

$$L = \frac{H}{\rho} \left\{ \sinh \frac{\rho x_B}{H} - \sinh \frac{\rho x_A}{H} \right\} \quad \text{A2.1}$$

where: L is the length of the cable
 H is the horizontal force in the cable
 ρ is the weight/unit length of the cable
 x_A, x_B are the x -coordinates of the cable ends (see figure below).



Parafil ropes are initially pulled handtight before testing. Assume, therefore, that $250 < H < 500$ (H in newtons).

$\rho = 7.1 \text{ N/m}$ (ICI trade literature)

The effective rope length $\approx 7400 \text{ mm} = 7.4 \text{ m}$.

Let $\ell = 7.4 \text{ m}$ then $(L - \ell)$ may be taken as an estimation of the initial slack.

Hence, $x_B = -x_A = 3.7 \text{ m}$

For $H = 250$ N, solve for L

$$L = \frac{250}{7.1} \left\{ 2 \sinh \left(\frac{7.1 \times 3.7}{250} \right) \right\}$$

$$\sinh(-x) = -\sinh(x)$$

$$= 7.414 \text{ m}$$

$$\text{Therefore slack} \approx 7414 - 7400 = 14 \text{ mm}$$

Similarly, for $H = 500$ N

$$L = 7.403 \text{ m}$$

$$\text{Therefore slack} = 7403 - 7400 = 3 \text{ mm.}$$

Hence, an offset on the X-axis of the load-displacement curves of between 3 mm and 14 mm would be expected when ropes are initially pulled handtight.

Appendix 3 The use of Coleman's model to predict the stress-rupture behaviour of Parafil directly

The governing differential equation from this model (Coleman and Knox 1957) is:

$$\frac{d\gamma}{dt} = \frac{2\lambda kT}{h} \exp \left\{ \frac{-E}{RT} \right\} \sinh \left\{ \frac{\sigma(t)\delta}{2kT} \right\} \quad A3.1$$

where: t is the time (secs)
 γ is the mean relative displacement of force centres at time t
 λ is the mean separation between the equilibrium positions of minimum force potential (m)
 k is Boltzmann's constant = 1.38066×10^{-23} JK⁻¹
 T is the absolute temperature (K)
 h is Planck's constant = 6.6262×10^{-34} JS
 E is the free energy of activation for the creep process (J mol⁻¹)
 R is the gas constant = 8.3144 JK⁻¹ mol⁻¹
 σ is the stress (Pa)
 δ is the product of the cross-sectional area per force centre and the jump distance (m³)

If Equation A3.1 is integrated subject to the conditions:

$$\begin{aligned} \text{at } t = 0 & \quad \gamma = 0 \\ \text{at } t = t_B & \quad \gamma = \gamma_B \end{aligned}$$

where: t_B is the time to break
 γ_B is the critical displacement of force centres

then under constant stress conditions (i.e. $\sigma(t) = \sigma$):

$$\gamma_B = \frac{2\lambda kT}{h} \exp \left\{ \frac{-E}{RT} \right\} \sinh \left\{ \frac{\sigma\delta}{2kT} \right\} t_B$$

Taking logarithms:

$$\ln \gamma_B = \ln \frac{2\lambda kT}{h} - \frac{E}{RT} + \ln \left[\sinh \left(\frac{\sigma\delta}{2kT} \right) \right] + \ln t_B$$

Now, σ must be $\gg \frac{2kT}{\delta}$ for failure to occur in a reasonable time

interval, and so:

$$\ln t_B = \ln \frac{h}{kT} + \ln \frac{\gamma_B}{\lambda} + \frac{E}{RT} - B\sigma \quad A3.2$$

where: $B = \frac{\delta}{2kT}$ (a constant)

The extrapolated zero strength temperature for Kevlar is approximately 913K (Wilfong and Zimmerman 1977); at this temperature, $\sigma = 0$ and polymer failure is instantaneous (say 1 second).

Also, γ_B/λ may be taken as the number of force-centre jumps to cause failure which is probably between 1 and 10 (Wilfong and Zimmerman 1977). The outcome is insensitive to γ_B/λ in this range, hence for mathematical convenience assume γ_B/λ equals exponential e (i.e. 2.718).

Substituting in Equation A3.2 therefore gives:

$$\ln 1 = \ln \left(\frac{6.6262 \times 10^{-34}}{1.38066 \times 10^{-23} \times 913} \right) + \ln e + \frac{E}{8.3144 \times 913} - 0$$

Therefore $E = 2.2 \times 10^5 \text{ J mol}^{-1}$.

For tests carried out at room temperature (293 K) Equation A3.2 now becomes:

$$\ln t_B = 63.723 - B\sigma \quad A3.3$$

If a stress is applied equal to the ultimate tensile strength of the material then the time to break should be almost instantaneous (say 1 second). Hence, from Equation A3.3:

$$\ln 1 = 63.723 - B\sigma_{UTS}$$

where: σ_{UTS} is the ultimate tensile strength.

$$\text{Therefore } B = \frac{63.723}{\sigma_{UTS}}$$

Hence, for constant loading conditions:

$$\ln t_B = 63.723 \left\{ 1 - \frac{\sigma}{\sigma_{UTS}} \right\} \quad A3.4$$

The resulting linear relationship (Equation A3.4) is plotted in Figure 7.3.

Appendix 4 The construction of lifetime probability curves for Parafil

The construction of these curves is based on the mathematical model for the stress-rupture behaviour of impregnated Kevlar 49 yarns in pressure vessel tests as described by Glaser et al.(1984).

A two-parameter Weibull distribution was assumed to be appropriate for describing the distribution of 'times to break' at any stress level. The distribution is of the form:

$$F(t_B) = 1 - \exp \left[- \left(\frac{t_B}{\beta} \right)^\alpha \right]$$

where: $F(t_B)$ is the cumulative distribution function of 'times to break'
 α is the Weibull shape parameter (stress-dependent)
 β is the Weibull scale parameter (stress-dependent).

Transforming the lifetime data into natural logarithms has the effect of reparameterising $\alpha(\sigma)$ and $\beta(\sigma)$ to $a(\sigma)$ and $b(\sigma)$ respectively (Glaser et al.1984)

Glaser et al.(1984) modelled the stress-dependency of a and b polynomially using θ coefficients. The θ coefficients were determined from the lifetime measurements using the method of maximum likelihoods (Cohen 1965).

Eight different spools of yarn were used in the fabrication of the pressure vessels and so the effect of spool variability was taken into consideration in determining the polynomial for b (see Equation A4.2). The most appropriate polynomial equations were found to be of the form:

$$a(\sigma) = \theta_1 + \theta_2 e^{2\sigma} \tag{A4.1}$$

$$b(\sigma) = \theta_3 \delta_1 + \theta_4 \delta_2 + \theta_5 \delta_3 + \theta_6 \delta_4 + \theta_7 \delta_5 + \theta_8 \delta_6 + \theta_9 + \theta_{10} \sigma \tag{A4.2}$$

where δ_1 accounts for the spool to spool variability.

For the worst fibre spool:

$$\delta_1 = \delta_2 = \delta_4 = \delta_5 = \delta_6 = 0, \delta_3 = 1$$

Substituting the appropriate values for Θ_i and δ_i and working in the units used in the paper by Glaser et al.(1984), (i.e. ksi and hours) gives:

$$a(\sigma) = 0.320037 + 1.51037 \times 10^{-4} e^{2\sigma} \quad A4.3$$

$$\begin{aligned} b(\sigma) &= -1.58238 + 31.5136 - 6.18686\sigma \\ &= 29.93122 - 6.18686\sigma \end{aligned} \quad A4.4$$

The maximum likelihood of the pth quantile of $\ln$ (lifetime) as a function of stress is given by:

$$\ln (t_B)_p = a \ln [- \ln (1 - p)] + b \quad A4.5$$

For a 5% probability of failure, $p = 0.05$. Substituting in Equation A4.5 gives:

$$\ln t_B = 28.98 - 4.49 \times 10^{-4} e^{2\sigma} - 6.19\sigma \quad A4.6$$

Similarly, for $p = 0.95$ (95% probability of failure):

$$\ln t_B = 30.28 + 1.66 \times 10^{-4} e^{2\sigma} - 6.19\sigma \quad A4.7$$

where: t_B is the lifetime in hours
 σ is the vessel pressure in ksi.

The vessel short-term burst pressure was found to be 5.03 ksi. Hence, converting Equations A4.6 and A4.7 to seconds and normalising with respect to short-term strength gives:

5% probability of failure:-

$$\begin{aligned} \ln t_B &= 37.17 - 4.49 \times 10^{-4} \exp \left\{ 10.06 \times \frac{\sigma}{\sigma_{UTS}} \right\} \\ &\quad - 31.12 \frac{\sigma}{\sigma_{UTS}} \end{aligned} \quad A4.8$$

where t_B is the time to break in seconds.

95% probability of failure:-

$$\ln t_B = 38.47 + 1.66 \times 10^{-4} \exp \left\{ 10.06 \times \frac{\sigma}{\sigma_{UTS}} \right\} - 31.12 \frac{\sigma}{\sigma_{UTS}} \quad A4.9$$

where t_B is the time to break in seconds.

Equations A4.8 and A4.9 are plotted in Figure 7.12.

A socially acceptable probability of failure for a structure is 10^{-6} (7.10). The equation for this probability of failure is given by:

$$\ln t_B = 25.51 - 20.87 \times 10^{-4} \exp \left\{ 10.06 \times \frac{\sigma}{\sigma_{UTS}} \right\} - 31.12 \frac{\sigma}{\sigma_{UTS}} \quad A4.10$$

where t_B is the time to break in hours.

Equation A4.10 (converted to base 10 logarithms) has been used in the computer program for determining suitable operating stresses for Parafil Type G ropes (7.10, 7.11).

Appendix 5 Cumulative damage and Coleman's approach to stress-rupture

To avoid rupture under Bailey's cumulative damage criterion (Bailey 1939):

$$\sum \frac{\Delta t_i}{(t_B)_i} < 1 \quad A5.1$$

where: Δt_i is an increment of time that the material spends under an essentially constant stress σ_i
 (σ is a function of time)
 $(t_B)_i$ is the stress-rupture lifetime corresponding to the stress σ_i .

The non-linear term in the LLNL model may be ignored for $\frac{\sigma}{\sigma_{UTS}} < 0.7$

(by inspection of Equations A4.8 and A4.9). Lifetime predictions from the LLNL model may, therefore, be expressed by the equation:

$$\ln t_B = \ln A - B\sigma \quad \sigma < 0.7 \sigma_{UTS} \quad A5.2$$

where: t_B is the time to break under a constant stress σ
 A and B are constants.

Substituting in Equation A5.1 for t_B gives:

$$\sum \frac{\Delta t_i}{(e^{\ln A - B\sigma})_i} < 1$$

now $e^{\ln A} = A$ and so to avoid rupture

$$\sum \frac{\Delta t_i}{A} e^{B\sigma_i} < 1 \quad A5.3$$

Coleman's governing differential equation (Coleman and Knox 1957) is:

$$\frac{d\gamma}{dt} = \frac{2\lambda kT}{h} \exp \left\{ \frac{-E}{RT} \right\} \sinh \left\{ \frac{\sigma(t)\delta}{2kT} \right\} \quad A5.4$$

(see Appendix 3 for definition of parameters).

Integrating subject to the conditions

$$\begin{aligned} \text{at } t = 0 \quad \gamma &= 0 \\ \text{at } t = t_f \quad \gamma &= \gamma_f \end{aligned}$$

[t_f is the time to failure under a stress history $\sigma(t)$]
gives:

$$\gamma_f \int_0^1 d\gamma = \frac{2\lambda kT}{h} \exp \left\{ \frac{-E}{RT} \right\} \int_0^{t_f} \sinh \left\{ \frac{\sigma(t)\delta}{2kT} \right\} dt$$

$$\text{Therefore } \frac{\gamma_f h}{2\lambda kT} \exp \left\{ \frac{E}{RT} \right\} = \int_0^{t_f} \sinh \left\{ \frac{\sigma(t)\delta}{2kT} \right\} dt$$

$$\text{Let } A = \frac{\gamma_f h}{\lambda kT} \exp \left\{ \frac{E}{RT} \right\} \quad (\text{constant})$$

$$\text{and } B = \frac{\delta}{2kT} \quad (\text{constant for constant temperature})$$

(A and B are the constants in the equation for constant loading:

$$\ln t_B = \ln A - B \sigma)$$

Substituting for A and B gives:

$$\frac{A}{2} = \int_0^{t_f} \sinh \{B\sigma(t)\} dt \quad \text{A5.5}$$

Equation A5.5 cannot readily be solved formally but can be solved numerically. Hence, to avoid rupture:

$$\sum [\sinh (B\sigma_i)] \Delta t_i < \frac{A}{2}$$

$$\text{Now } \sinh x = \frac{e^x - e^{-x}}{2} \quad \text{and so:}$$

$$\sum \left[\frac{e^{B\sigma_i} - e^{-B\sigma_i}}{2} \right] \Delta t_i < \frac{A}{2}$$

BQ_i is typically large and so $e^{-BQ_i} \ll e^{BQ_i}$. Hence we may write:

$$\sum e^{BQ_i} \Delta t_i < A \quad \text{or}$$

$$\sum \frac{\Delta t_i}{A} e^{BQ_i} < 1 \quad \text{A5.6}$$

Equations A5.3 and A5.6 are identical. The Coleman model is, therefore, entirely consistent with the concept of cumulative damage.

Appendix 6 Listing of DAM6 computer program and typical output

LIST

```

10 REM PROGRAM DAM6
20 REM THIS PROGRAM DETERMINES INITIAL PRESTRESS FOR PARAFIL
30 VDU2
40 PRINT"MINIMUM, TYPICAL, OR MAXIMUM LOSSES MAY BE CONSIDERED"
50 REM PRESTRESS LOSSES ARE DESCRIBED IN SECTION 7.9
60 PRINT"ENTER 1.05,1.45 OR 2.14 FOR MIN, TYP, OR MAX RESPY"
70 INPUT X
80 PRINT"ENTER TIME INCREMENT FOR FIRST YEAR IN DAYS"
90 REM A SUITABLE INCREMENT IS 7 DAYS
100 INPUT TI
110 A=365-TI
120 PRINT"ENTER REMAINING TIME INCREMENT IN DAYS"
130 REM A SUITABLE INCREMENT FOR REMAINDER IS 182 DAYS
140 INPUT TIR
150 PRINT"ENTER INITIAL % NBL"
160 INPUT FMAX
170 REM INITIALISE DAMAGE, TIME
180 DAM=0:T=0
190 S=FMAX
200 S=S/100
210 REM USE LIFETIME EQUATION FOR 1 IN 10^6 FAILURE PROBABILITY
220 LT=11.08-9.06E-4*2.71828^(10.06*S)-13.52*S
230 L=10^LT
240 REM SUM THE DAMAGE
250 DAM=DAM+TI*24/L
260 REM STATE FAILURE CRITERION
270 IF DAM >=1.0 THEN 350
280 T=T+TI*24
290 IF T>=A THEN TI=TIR
300 REM STOP IF FAILURE HAS NOT OCCURRED WITHIN 100 YEARS
310 IF T>=876600 THEN 350
320 REM DETERMINE STRESS FOR NEXT TIME INCREMENT
330 S=FMAX-(3.63+X*LOG(T))
340 GOTO 200
350 PRINT"DAMAGE SUSTAINED = ";DAM
360 B=T/8766
370 PRINT"TIME ELAPSED IN YEARS = ";B
380 PRINT:PRINT:PRINT
390 VDU3
400 PRINT"DO YOU WANT TO TRY AGAIN? Y/N"
410 INPUT A$
420 IF A$="Y" THEN 430 ELSE 450
430 VDU2
440 GOTO 80
450 END

```

MINIMUM, TYPICAL, OR MAXIMUM LOSSES MAY BE CONSIDERED
 ENTER 1.05,1.45 OR 2.14 FOR MIN, TYP, OR MAX RESPY
 ?1.05
 ENTER TIME INCREMENT FOR FIRST YEAR IN DAYS
 ?7
 ENTER REMAINING TIME INCREMENT IN DAYS
 ?182
 ENTER INITIAL % NBL
 ?51
 DAMAGE SUSTAINED = 1.00674244
 TIME ELAPSED IN YEARS = 21.4839151

MINIMUM, TYPICAL, OR MAXIMUM LOSSES MAY BE CONSIDERED
 ENTER 1.05,1.45 OR 2.14 FOR MIN, TYP, OR MAX RESPY
 ?1.45
 ENTER TIME INCREMENT FOR FIRST YEAR IN DAYS
 ?7
 ENTER REMAINING TIME INCREMENT IN DAYS
 ?182
 ENTER INITIAL % NBL
 ?49
 DAMAGE SUSTAINED = 0.941722121
 TIME ELAPSED IN YEARS = 100.213552

MINIMUM, TYPICAL, OR MAXIMUM LOSSES MAY BE CONSIDERED
 ENTER 1.05,1.45 OR 2.14 FOR MIN, TYP, OR MAX RESPY
 ?2.14
 ENTER TIME INCREMENT FOR FIRST YEAR IN DAYS
 ?7
 ENTER REMAINING TIME INCREMENT IN DAYS
 ?182
 ENTER INITIAL % NBL
 ?54
 DAMAGE SUSTAINED = 1.0028377
 TIME ELAPSED IN YEARS = 55.3675565

Appendix 7 Prestress fluctuation due to temperature changes in the British Isles

The maximum and minimum shade air temperatures to be found in the British Isles are +37°C and -24°C respectively (Department of Transport 1977).

It is not unusual for limitations to be applied to the temperatures at which a civil engineering contractor may perform certain site operations. For example, it is normal to limit the temperature at which bridge expansion joints may be installed to the range +4°C to +23°C (Clark 1978). The fluctuation in prestress due to temperature changes would be reduced if the same limitations were applied when stressing Parafil Type G tendons in the British Isles.

If the tendons are stressed at 4°C:

the concrete could expand due to $(37 - 4) = 33^{\circ}\text{C}$

the concrete could contract due to $(4 - -24) = 28^{\circ}\text{C}$.

If the tendons are stressed at 23°C:

the concrete could expand due to $(37 - 23) = 14^{\circ}\text{C}$

the concrete could contract due to $(23 - -24) = 47^{\circ}\text{C}$.

The stress-change in a Parafil Type G tendon due to a temperature fluctuation is given by:

$$\Delta\sigma = E. (\alpha_c - \alpha_p). \Delta t \qquad \text{A7.1}$$

where: $\Delta\sigma$ is the change in stress (MPa)

E is the elastic modulus of Parafil Type G (MPa)

α_c is the coefficient of longitudinal thermal expansion for concrete (/°C)

α_p is the coefficient of longitudinal thermal expansion for Parafil (/°C)

Δt is the change in temperature (°C).

$$\begin{aligned} \text{Assume: } (\alpha_c - \alpha_p) &= 12 \times 10^{-6} - (-2 \times 10^{-6}) \\ &= 14 \times 10^{-6} \text{ /}^{\circ}\text{C} \end{aligned}$$

and that $\Delta t = 33^{\circ}\text{C}$ for maximum concrete expansion
 $\Delta t = 47^{\circ}\text{C}$ for maximum concrete contraction.

Hence, the maximum stress increase in a Parafil Type G tendon is given by (Equation A7.1):

$$\begin{aligned}\Delta\sigma &= 1.18 \times 10^5 \times 14 \times 10^{-6} \times 33 \\ &= 54.5 \text{ MPa}\end{aligned}$$

and the maximum stress decrease is given by:

$$\begin{aligned}\Delta\sigma &= 1.18 \times 10^5 \times 14 \times 10^{-6} \times 47 \\ &= 77.6 \text{ MPa.}\end{aligned}$$

The stress increase and decrease correspond to 2.8% NBL and 4.0% NBL respectively.

Appendix 8: The determination of the prestress force and the degree of bond during the ultimate test on the prestressed concrete beam

To be able to predict accurately the ultimate moment of resistance for an unbonded prestressed concrete beam a measure of the friction developed between the unbonded tendon and the prestressing duct is required. For a steel tendon an equivalent bond factor of 0.1 is commonly used as this accounts reasonably well for the observed increased steel strain at failure.

To determine the bond factor for the Parafil tendon used in the beam test (Chapter 9) a knowledge of the force in the tendon just prior to beam failure is required. Direct measurement of this force was not practicable but it can be derived from the strain distribution across the beam. The strain distribution was given by the strain in the untensioned longitudinal steel as measured throughout the test by electrical resistance strain gauges. From the strain distribution the shape and magnitude of the compressive stress block in the concrete can be determined if an assumption about the stress-strain characteristic of the concrete is made. Here, a stress-strain curve for concrete as proposed by Desayi and Krishnan (1964) is employed. Similarly, using known stress-strain characteristics for the untensioned steel the force in the top and bottom longitudinal steel can be calculated. A correction factor was applied to the concrete stress block to give moment equilibrium (since the applied bending moment was known) and then the force in the rope was determined from a consideration of force equilibrium. A computer program incorporating this technique was written to calculate the force in the Parafil rope throughout the last load cycle applied to the beam. Stress-strain curves for concrete, particularly when applied to a beam in flexure, must be regarded as approximate and consequently the increases in rope stress calculated must be treated as estimates.

The proposed stress-strain curve for concrete is shown in Figure A8.1 and is described by the equation:

$$f = \frac{E \epsilon}{1 + (\epsilon / \epsilon_0)^2} \quad \text{A8.1}$$

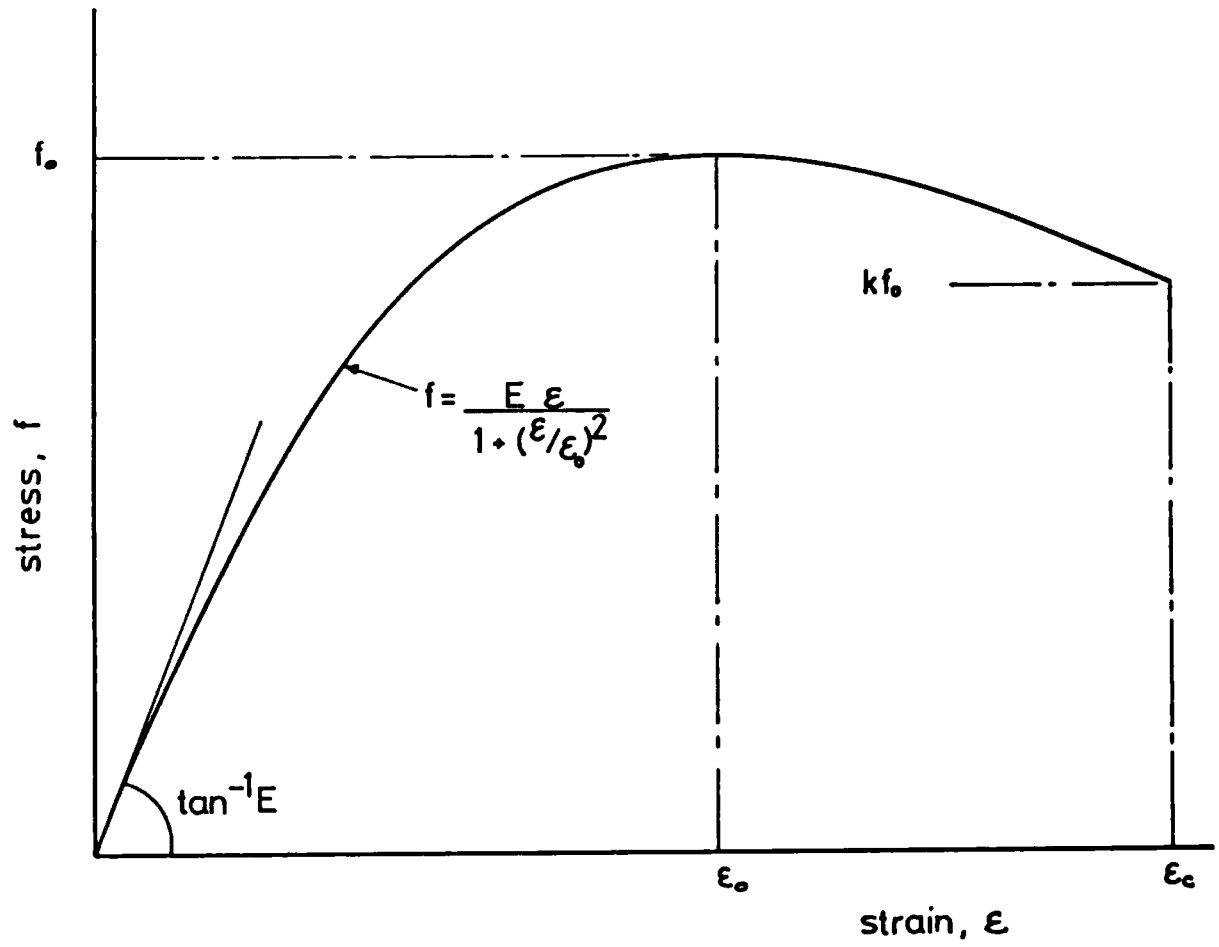


Figure A8.1 Proposed stress-strain curve
Desayi and Krishnan (1964).

where: f is the stress at any strain ϵ
 ϵ_0 is the strain at the maximum stress of f_0
 E is a constant (same as initial tangent modulus)
 such that $E = 2f_0/\epsilon_0$.

Desayi and Krishnan (1964) also define:

kf_0 as the stress at failure and
 ϵ_c as the maximum strain at failure.

Desayi and Krishnan (1964) suggest that if an assumption is made about kf_0 and that E is taken as $2f_0/\epsilon_0$ then ϵ_0 is determinable. A value for k is typically 0.85 and ϵ_c is generally taken as 0.0035.

Substituting in Equation A8.1:

$$0.85 f_0 = \frac{2f_0 / \epsilon_0 \times 0.0035}{1 + (0.0035/\epsilon_0)^2}$$

Therefore

$$0.85 = \frac{2/\epsilon_0 \times 0.0035}{1 + (0.0035/\epsilon_0)^2}$$

This equation is satisfied by $\epsilon_0 = 0.00195$.

A value for f_0 is found as follows:

cube strength $f_{cu} = 57.5$ MPa (match cured specimens)

corresponding cylinder strength $f_c = 0.78 f_{cu}$

maximum stress reached $f_0 = k_3 f_c$

Table 3.1 from Park and Paulay (1975) gives $k_3 = 0.93$ for this strength concrete.

Hence $f_0 = 0.93 \times 0.78 \times 57.5 = 41.7$ MPa

It was assumed that $E = 2f_0/\epsilon_0$
 $= 2 \times 41.7/0.00195$
 $= 42769$ MPa

An 'E' test was performed on a match-cured specimen and gave a result of 35800 MPa. Furthermore, a comparison between Desayi and Krishnan's predicted stress-strain curve and those observed in tests on high strength concretes shows that the predicted curve over-estimates the initial tangent modulus as also indicated here.

Instead, assume that $f_0 = 41.7$ MPa and that $E = 35800$ MPa and substitute directly into the proposed equation (Equation A8.1) to find ϵ_0 .

$$\begin{aligned} 41.7 &= \frac{35800 \times \epsilon_0}{1 + (\epsilon_0/\epsilon_0)^2} \\ &= 41.7 \times 2/35800 \\ &= 0.00233 \end{aligned}$$

This calculation for ϵ_0 is probably more reliable since it does not depend on assumptions for kf_0 , ϵ_c or E . To describe the stress-strain curve for the concrete, therefore, the following equation has been adopted in the computer program:

$$f = \frac{35800 \times \epsilon}{1 + (\epsilon/0.00233)^2}$$

Table A8.1 shows the estimated increase in prestressing force with increasing applied load for load cycle 3; the results are also shown in Figure A8.2. The correction factor c , in Table A8.1, is the value by which the resultant force from the assumed stress block had to be multiplied to give moment equilibrium. The proximity of the factor to unity for each load-case considered indicates that Desayi's proposed stress-strain curve for concrete gives satisfactory though not exact results.

The theory actually predicts a slight fall in prestress from the initial value at the lower loads which would seem to support the evidence of a prestress loss during the previous load cycle (9.7). As expected, the increase in prestress for working loads is negligible. It is only when the section becomes cracked and large deformations result that the prestress rises rapidly. The prestress at failure was estimated to be 507.4 kN, i.e. 30.4% higher than the initial force.

load (kN)	live load bending moment (kN-m)	% increase based on initial prestress of 389.1 kN	correction factor, c
29.15	25.30	-4.0	0.84
57.29	49.73	-0.3	0.86
74.75	64.89	-1.0	0.88
105.91	91.93	-2.1	0.90
120.76	104.82	0.8	0.94
137.70	119.53	6.4	0.97
150.25	130.42	11.1	0.99
155.92	135.34	14.6	0.97
162.47	141.03	18.9	0.96
169.06	146.75	23.1	0.94
173.43	150.54	26.9	0.93
177.36	153.95	30.4	0.91

Table A8.1 Increase in prestress during ultimate load test.

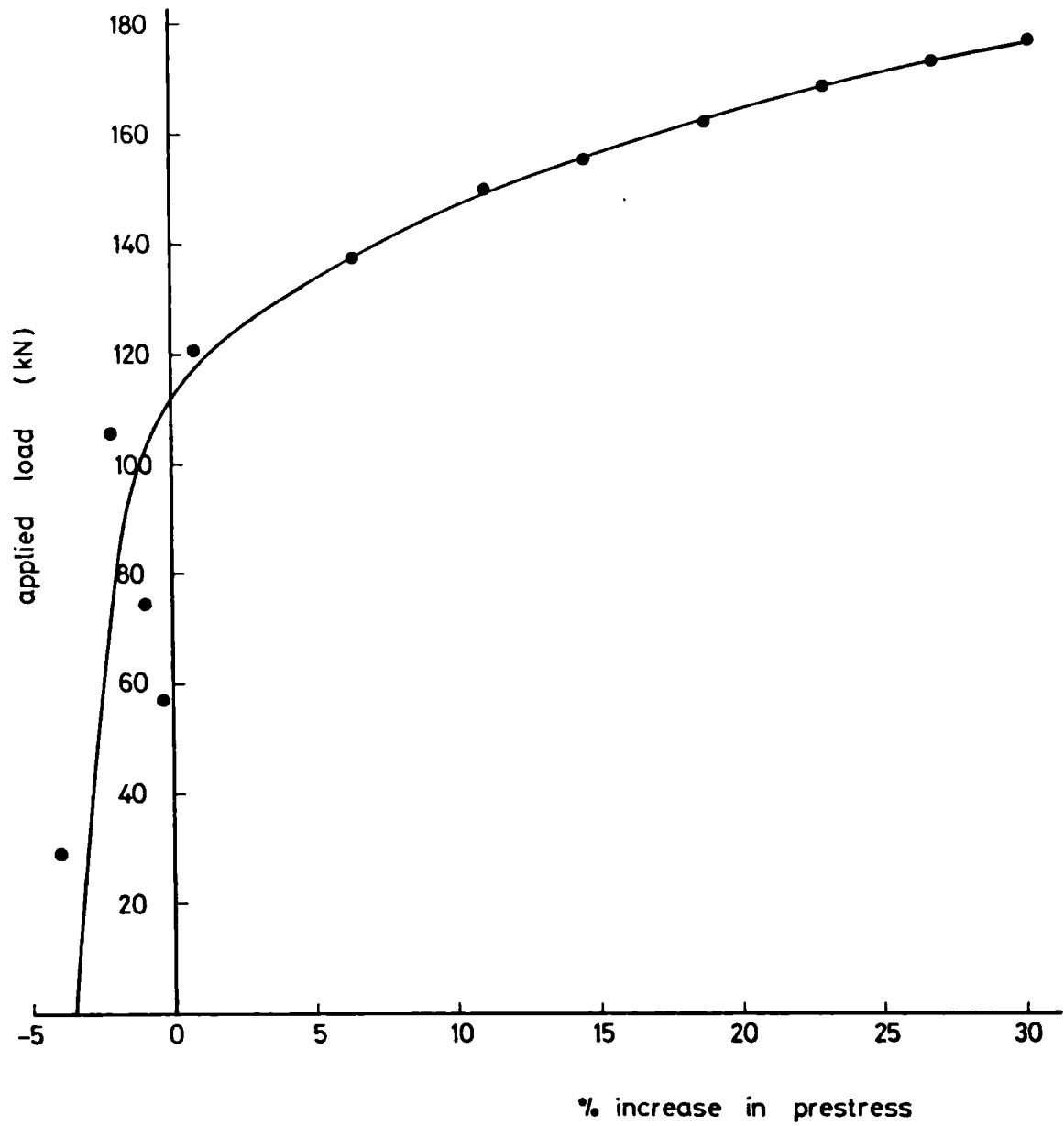


Figure A8.2 Increase in prestress force during final load cycle.

A standard iterative procedure, of which the last iteration is shown below, may now be used to determine the equivalent bond factor, β_2 , that must have prevailed under ultimate conditions. A simplified rectangular stress block is used in this case.

neutral axis depth	63 mm
effective prestress strain	0.0106 (assumes $E_{\text{Parafil}} = 1.2 \times 10^5$ MPa and 6.25% relaxation loss)
$\beta_2 \cdot \epsilon_u$	to be determined (ϵ_u = concrete strain at tendon level)
total strain in Parafil	0.0138
Parafil stress	1656 N/mm ²
Parafil force	507 kN
compression in concrete	542 kN (simplified stress block)
compression in top steel	37 kN
tension in bottom steel	77 kN
total compressive force	579 kN
total tensile force	586 kN

The strain ϵ_u (concrete strain at tendon level) is given by $242 \times 0.0035/63 = 0.0134$. Hence:

$$\beta_2 \times 0.0134 = 0.0138 - 0.0106$$

$$\text{Therefore } \beta_2 = 0.24$$

Incidentally, the above calculation predicts an ultimate moment of resistance of 164.9 kN-m, c.f. 158.1 kN-m as recorded which indicates that the assumptions are reasonable.

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