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DEVELOPMENT OF BREAKAWAY UTILITY POLES

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Abstract

An investigation of alternative schemes for in-situ modification of existing conductor-carrying and luminaire timber poles was undertaken using a 1:7.38 scale model of the vehicle/pole/cable system. A simple pole modification scheme which achieved a significant reduction in collision severity while maintaining the integrity of the conductors was developed.

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DEVELOPMENT OF BREAKAWAY
UTILITY POLES

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Department of Mechanical Engineering
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December, 1979

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SUMMARY

This report describes an investigation of alternative schemes for in-situ modification of existing conductor-carrying and luminaire timber poles, with the objective of reducing the severity of vehicular impact while maintaining the integrity of the conductor cables.

The feasibility of modifying Australian timber utility poles was investigated initially by a simplified mathematical model. The model results showed that significant reductions in collision severity could be achieved through pole modification.

A 1:7.38 scale model test facility was built to further investigate the breakaway utility pole concept. In all, 95 impact tests were conducted on a variety of pole modification schemes, configurations and timbers under a range of impact conditions. The test results confirmed the mathematical model predictions, and a simple pole modification scheme was developed that significantly reduced collision severity over the range of test conditions. Conductor cable tensions resulting from impact and pole breakaway remained well within the cable ultimate strength for both the single- and two-crossarm pole tests.

For all timber pole configurations, the recommended modification at the pole base consists of two layers of crossed-holes connected by vertical slots. In the case of two-crossarm poles a second pole modification consisting of crossed-holes just below the lower crossarm is recommended. No upper pole modification is required for the single-crossarm pole configuration. Instead, the pole and crossarm strip easily away from the conductors by breaking the cable tie wires.

The installation of restraining cables between poles for all configurations will prevent secondary collisions between the vehicle and breakaway pole for all but the lowest speed impacts.

Curves showing the expected crash severity plotted against rated
(design) pole strength are presented for both ironbark and messmate poles modified at the base by slots. The curves show that impacts with modified ironbark poles up to a rated strength of 6 kN, and modified messmate poles up to 8 kN, result in vehicle impulse levels which satisfy the TF no-injury criterion. All modified poles in the range tested (up to 8 kN) result in collision severities less than the adopted severe or fatal injury limits.

A small number of full scale validation tests are recommended. Development of stronger cable/insulator connections is required for two-crossarm configurations.

* Transportation Research Board
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Mr. Rod Howard and Mr. John Howell contributed significantly to the project with their technical expertise and inventiveness.

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1.1 INTRODUCTION

Crashes with roadside poles are a significant source of death, injury and economic loss in Australia. In the Melbourne metropolitan area alone, the survey by Fox, Good and Joubert (1979) revealed that 45 people are killed and 785 injured each year in pole crashes. These casualties represent 9.4 percent of deaths and 5.9 percent of injuries from all road accidents in the same area. In terms of non-pedestrian accidents, about 15 percent of vehicle occupant fatalities in Melbourne occur in pole crashes.

Of the poles struck in the Melbourne metropolitan area, approximately 55 percent are timber poles which support overhead conductors. Crashes with these poles cost the community $12.7 million annually, when property damage, medical and rehabilitation costs and the value of lost production are accounted for (Fox et al., 1979).

A number of measures are available for reducing the social cost arising from pole crashes. Fortunately, the magnitude of the remedial program to be undertaken is reduced by the fact that a relatively small number of poles are associated with a majority of accidents. Fox et al., derived a predictor model which allows the identification of these high-risk poles from simple site measurements. The economic costs and benefits of pole or site modifications may also be predicted.

Street lighting poles must of necessity be in the vicinity of the roadway, although their location should be chosen so as to minimize risk. For those which are exposed to accident risk, proven and economical breakaway and collapsible designs are available. For poles which have no function but to support conductors, the 'ideal' solution from a safety point of view is removal or relocation to less hazardous positions. For a number of high-risk poles
this is an economically feasible solution. For a larger number of 'intermediate risk' poles, however, the alternative approach of attenuating the collision severity warrants consideration.

Fox et al., have shown that conventional impact attenuation devices, such as steel beam guardrails or collapsible crash cushions, cannot be used to reduce pole crash severity on the urban road system. On the other hand, little improvement can be expected in vehicle crashworthiness in pole collisions, especially given the trend towards smaller and lighter vehicles. The remaining option is modification of the pole itself. This report describes an investigation of a scheme for in-situ modification of existing, conductor-carrying timber poles, with the objective of reducing the severity of vehicular impact.

1.2 BREAKAWAY CONCEPT FOR TIMBER UTILITY POLES

The feasibility of breakaway, or frangible base designs for fixed roadside hazards, such as lighting standards and sign supports, has been well known since the early 1960's. They have been widely used in the United States and Europe. These designs present a lower shear strength zone near ground level to the errant vehicle, while maintaining the required bending, compression and shear strengths to withstand service loads, under design environmental conditions.

The criteria on which the success of a design is judged are:

(a) The vehicle velocity change and deceleration during impact.  
(A review of the literature on the relationship between occupant injury and vehicle frame impact response is presented in Appendix C.)

(b) The final resting place of the debris.

(c) The applicability to existing installations.

(d) The structural integrity under service loads.

(e) Costs.

Designs for sign and luminaire supports have been thoroughly researched and documented. This is not the case for cable-supporting poles, hereafter referred to as 'utility poles'. In the case
of the utility pole there is an additional consideration to the previously listed five:

(f) The integrity of the conductors and insulators should be maintained during impact.

1.2.1 The Crossed-Holes Concept

The basic work on the feasibility of modifying in-service timber utility poles to meet the requirements outlined above has been carried out in the United States by Wolfe, Bronstad, Michie and Wong (1974).

Given the very large population of existing poles, economic realities dictate consideration of solutions involving simple and cheap modifications to in-service poles. To minimise the weight supported by the cables and adjacent poles after an impact, Wolfe et al., proposed the creation of two weakened shear zones in the pole, one near the ground and one near the top. During an impact it is intended that the centre section 'pop out' leaving the cables and cross-arms intact. Their method was to drill and cut a pattern of holes and grooves 15 cm from the groundline and 1.83 m from the top of a 12 m pole.

Figure 1.1 details the lower zone modification. The size of the holes was determined so as to limit the linear impulse resulting from impact, while maintaining the utility authorities' specifications for resisting in-service loads.

Thirteen pendulum tests were carried out on modified 12 m poles, the pole material being creosote-treated Southern Pine. A schematic of the impact sequence is shown in Figure 1.2.

The test results demonstrated the feasibility of the breakaway timber utility pole concept, for the particular timber type used in the test program. It is emphasized by Wolfe et al., that 'before the concept is validated for in-service trial use, a matrix of vehicle crash tests should be conducted to demonstrate the concept performance under actual conditions'.
Figure 1.1. Two-hole breakaway pole concept (Wolfe et al., 1974)

Figure 1.2. Utility pole fracture sequence (Wolfe et al., 1974)
5.

Direct extrapolation of even the very basic work done by Wolfe et al., to Australian conditions is perilous for a number of reasons:

(a) The average Australian pole timber is between 50 - 100% stronger than that tested by Wolfe et al. (Boyd, 1961, 1962a and b, 1964, 1968).

(b) Different cable types, materials and configurations are used in Australia.

(c) The in-service pole strength requirements are more stringent in Australia.

(d) The extrapolation of rigid or deformable-nose pendulum test results to predict system responses involving deformable vehicles is not well founded (Bronstad, Michie and White, 1976; Chisholm and Viner, 1973).

(e) The distribution of the population of Australian vehicle weights has a lower mean than the American distribution, which would result in Australian vehicles typically undergoing greater velocity changes for a given strength frangible base.

1.2.2 The Slot-Shim Concept

Labra (1977) extended the preliminary work of Wolfe et al., and evaluated a range of potential retrofits, including bore-hole mechanisms, extensive cutaway slot mechanisms and a staggered slot/shim concept. The pole-cable configuration investigated consisted of four conductor cables and a single cross-arm which supported three of the conductor cables, with the remaining cable being attached to the pole itself.

After a series of static pole bending tests and pendulum impact tests, the base modification system recommended by Labra was the staggered slot/shim concept (Figure 1.3). This modification is effected by the sawing of three staggered slots into the pole to a prescribed depth. Plywood shims are then inserted in the slots to partially restore the bending moment capability of the pole without increasing its impact resistance. The slots were staggered, rather
Figure 1.3. Staggered slot/shim concept (Labra, 1977).

Section A-A

All dimensions in mm

G D I A

D, DIA.

A

75

75

25

Ground line
than being in one plane, after it was noticed that the poles split vertically during pendulum tests of the original concept. It was thought that staggering of the slots would cause the timber to strip its fibres from the edge of one slot to another during impact. This was subsequently observed to happen.

The upper pole modification used initially by Labra was similar to that adopted by Wolfe et al., i.e., two crossed holes. However, difficulty was encountered during the pendulum tests in achieving failure at the upper modified zone, due to premature failure of the conductor ties (wire ties which attach the conductors to the insulators). It was thought that the in-service ties may be strong enough to alleviate the problem in the subsequent full-scale tests. (Apparently the ties used by Labra were non-standard.)

Following the static and pendulum test evaluations of various pole modification schemes, Labra conducted three full scale tests in which 1972 Chevrolet Vegas were crashed into poles modified by staggered slot/shims at the base.

The first test was conducted at an impact velocity of 20 mph (32.2 km/h) and resulted in a successful detachment of the pole from the base at the lower modified zone. However, the upper 'crossed-holes' modified zone failed to work and the pole remained attached to the conductor cables, dragging over the bonnet and windscreen as the vehicle continued on. As this happened, the weight of the pole caused it to fall, dragging the cables down until the tie wires failed. The free pole then fell onto the vehicle roof, inflicting only minor damage.

The second test was similar to the first, except for the use of lighter gauge conductor cables. Once again the upper pole modification failed to activate because of the low impact velocity, although in this test the breakaway pole remained suspended by the conductor cables.

For the third and final test the 'crossed-holes' upper modification scheme was replaced by a cross-arm release mechanism which allows the outer, cable-supporting portions of the cross-arm to release from the pole, and the central part of the cross-arm, during
impact. This test was successful. The 61.8 mph (99.4 km/h) impact resulted in a 12 mph (20 km/h) velocity change and an average vehicle deceleration of 17 g. No mention was made in Labra's report of the pole trajectory in relation to the vehicle, or the final resting place of the pole.

Extrapolation of Labra's work to the Australian scene is subject to the same qualifications outlined earlier in relation to the work of Wolfe et al. However, three further points arise concerning the acceptance of Labra's modification scheme by utility authorities:

(a) The slot/shim system relies heavily on the correct and continued installation of the plywood shims. Should a shim be dislodged in some way, the load-bearing capacity of the pole would be significantly reduced. The possibility of such dislodgements would need to be guarded against.

(b) The effect of weathering and ageing on the slot/shim system should be considered. Any loss of contact between the shim and the pole due to degradation of the shim or the surface of the slot would also lead to reduced load-bearing capacity.

(c) The cross-arm release mechanism may not be applicable to poles with two cross-arms (and hence two layers of cables) because of the possibility of the falling pole and cross-arm segments snagging the conductor cables on the way down.

Despite reservations concerning the detailed applicability of the American results to Australian conditions, it is clear that the concept of breakaway utility poles is worthy of further investigation.

1.3 MATHEMATICAL SIMULATION OF IMPACT WITH BREAKAWAY POLE

1.3.1 Basis of Model

The time and expense involved in performing a 'matrix' of full-scale crash tests on a variety of pole modifications to establish the optimum design of a breakaway pole makes such an approach impractical. A parameter study of the type recommended
by Wolfe et al., (1974) is, however, a necessary prelude to in-service tests of a proposed impact attenuation scheme.

An alternative approach to a 'matrix' of expensive full-scale crash tests is to eliminate the less important design parameters from the matrix, and to perform the basic design development by a simulation of the vehicle-pole-cable interactions.

Simulation can be performed by:

(a) Mathematical modelling and numerical experiments using a computer, and

(b) Scale model testing.

The feasibility of a computer simulation has been investigated using a finite-element approach. It was concluded that to adequately model the dynamics of complex pole-cable systems with geometric and material non-linearities is prohibitively elaborate and expensive. Further empirical information is required on the behaviour of such systems before appropriate simplifications can be made to the detailed mathematical model. A much simplified mathematical model of a vehicle-pole impact has therefore been derived to enable an initial exploration of the effect on vehicle deceleration and velocity change of breakaway pole parameters, employing Australian pole configurations and timbers.

The model, depicted in Figure 1.4, represents a vehicle of mass \( M \), with a constant crush stiffness \( k \), colliding at speed \( V \) with a cable-supporting pole with a base shear strength \( F_s \) and moment of inertia about the top of the pole of \( I \). The assumptions employed in the formulation of the equations of motion are as follows:

(a) The vehicle has a crush characteristic with zero coefficient of restitution.

(b) The pole base shears instantaneously when the force between the pole and vehicle reaches \( F_s \).

(c) After shearing, the pole rotates about point \( O \), a pin joint at the top of the pole.

(d) Flexibility of the pole is neglected.
Phase I - vehicle crush with no pole displacement

Phase II - pole free to rotate about 0

Figure 1.4. Rigid pole mathematical model
11.

The model describes two distinct phases: Phase I involves vehicle crush with no pole displacement. **Phase II, in which the pole is accelerated from rest and is free to rotate about 0**, begins when the force between the vehicle and the pole has risen to the pole shear strength $F_s$.

It is thought that the assumption of a pin joint at $0$ is not critical, in terms of vehicle motions, for two reasons:

(a) It can be expected that most of the velocity change of the vehicle will occur during Phase I. During the short additional period of deceleration in Phase II, the pole displacement will be small, and the details of fixity at the top unimportant.

(b) With practical cable sags, a small displacement at the top of the pole results in a rapid rise in cable tensions, which restricts translation of the top section of the pole.

The derivation of the model is detailed in Appendix A. Major results only are presented here.

### 1.3.2 Numerical Evaluation

The baseline parameter values in Table 1.1 are representative of poles and vehicles on urban arterial roads. The base shear strength is for a cross-section which satisfies the bending strength requirement of the State Electricity Commission of Victoria (SECV, 1977) for an '8 kN pole'. (Poles are classified according to the maximum lateral force which can be applied near the top of the pole.)*

For these baseline values the vehicle response predicted is:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Value</th>
</tr>
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<tr>
<td>Maximum crush, $\delta$</td>
<td>$0.95$ m</td>
</tr>
<tr>
<td>Maximum deceleration, $\dot{x}$</td>
<td>$25.7$ g</td>
</tr>
<tr>
<td>Velocity change, $\Delta v$</td>
<td>$7.81$ m/s</td>
</tr>
</tbody>
</table>

* The baseline value of 8 kN was selected on the basis of SECV advice that this was representative of poles on major urban routes. It was subsequently discovered, however, that 8 kN exceeds the strength for withstanding the SECV design loads for most of these poles. (**See Chapter 2**.)
Given the baseline pole inertia, the lower limit of crash severity can be obtained by setting the base strength $F_s$ to zero, with the following results:

$$\delta_{\text{max}} = 0.44 \text{ m}$$
$$\ddot{x}_{\text{max}} = 12.0 \text{ g}$$
$$\Delta V = 2.50 \text{ m/s}$$

**TABLE 1.1**

**MATHEMATICAL MODEL BASELINE PARAMETER VALUES**

<table>
<thead>
<tr>
<th>Variable</th>
<th>Value</th>
<th>Source</th>
</tr>
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<tr>
<td>Base shear strength</td>
<td>$F_s$</td>
<td>320 kN</td>
</tr>
<tr>
<td>Pole moment of inertia</td>
<td>$I$</td>
<td>$1.76 \times 10^4 \text{ kg.m}^2$</td>
</tr>
<tr>
<td>Vehicle crush stiffness</td>
<td>$k$</td>
<td>358 kN/m</td>
</tr>
<tr>
<td>Pole height</td>
<td>$H$</td>
<td>9.8 m</td>
</tr>
<tr>
<td>Vehicle mass</td>
<td>$M$</td>
<td>1343 kg</td>
</tr>
<tr>
<td>Impact speed</td>
<td>$V$</td>
<td>20.75 m/s</td>
</tr>
</tbody>
</table>

By comparison, an unmodified pole would result in:

$$\delta_{\text{max}} = 1.27 \text{ m}$$
$$\ddot{x}_{\text{max}} = 34.7 \text{ g}$$
$$\Delta V = 20.75 \text{ m/s}$$

It can be seen that the main benefit of the pole modification is a marked reduction in the velocity change. The benefit of a reduction in velocity change in terms of a lower occupant injury probability needs to be evaluated. Appendix C presents a review of the literature on the relationship between occupant injury level
and vehicle deceleration and velocity change. The review showed that injury tolerance limits are not well established. However, for the purposes of design evaluation the data of Chi (1976) and that of the Transportation Research Board (1978) were chosen as being representative of the severe injury and injury threshold limits respectively. The data presented by Chi for restrained occupants quoted an average deceleration of 25 g and a velocity change of 12 m/s as the limits for fatal or irreversibly disabling injuries.

The Transportation Research Board in the latest procedure guide for the crash testing of highway appurtenances recommends a velocity change limit of 4.8 m/s (impulse of 4890 Ns) for light cars (1020 kg) and unrestrained occupants as the injury threshold. The results of the modified impact (14.4 g average deceleration and 7.81 m/s velocity change) are below Chi's limits, and above the TRB no-injury limit, which is a marked improvement compared with the unmodified pole impact results which were well above Chi's fatal or irreversibly disabling injury limits.

Figures 1.5 - 1.9 show the effect on the predicted peak vehicle deceleration ($\ddot{x}_{\text{max}}$) and velocity change ($\Delta V$) of variations in $I$, $V$, $F_s$, $M$ and $k$. The baseline value of each of these parameters is plotted on each graph.

Figure 1.5 demonstrates that most of the vehicle deceleration and velocity change results from shearing the pole. Increasing pole inertia produces only small increases in $\Delta V$ or $\ddot{x}_{\text{max}}$.

Impact velocity, while only affecting peak deceleration marginally, has a marked influence on velocity change. For the baseline values chosen, Figure 1.6 shows that the pole does not shear for impact velocities less than 14.6 m/s (52 km/h). For impact velocities ranging from this lower limit up to 30 m/s, velocity change decreases. For impact speeds higher than 30 m/s, $\Delta V$ increases again. This phenomenon may be explained as follows. The velocity change during the pole shear phase decreases as the impact speed increases. This is because a given base shear force demands a constant maximum vehicle crush force, and hence a constant loss of kinetic energy in deforming the vehicle. The vehicle velocity
Figure 1.5. Effect of pole inertia on crash severity

Figure 1.6. Effect of impact velocity on crash severity
change required to provide this energy is less at higher speeds. Velocity change during phase II, when the sheared pole is being accelerated, increases with increasing initial velocity. However for impact velocities up to 30 m/s, the reduction in velocity change during phase I predominates. At higher speeds the increase in velocity change during phase II results in an overall increase in $\Delta V$. This type of behaviour is in accord with the results presented by Edwards et al., (1969), in their study of slip-base luminaires.

The effect of pole shear strength is shown clearly in Figure 1.7. Large increases in velocity change and peak deceleration result from increases in $F_s$. The baseline value of $F_s$ shown is the minimum that can be achieved by drilling out the pole, as in Figure 1.1, while maintaining the specified service strength of an 8 kN pole. $F_s$ was calculated on the basis of the shear area of the pole remaining after modification (modified to an 8 kN strength rating) and an estimate of the material shear strength reported by Boyd (1968) chosen as $1.557 \times 10^7$ Nm$^{-2}$. This value has a degree of uncertainty associated with it, and the results of the model must therefore be considered as indicative of trends rather than exact values. Also shown on Figure 1.7 are the shear strengths corresponding to lower pole strength ratings. It would appear that if the pole could be modified to rated strengths of 4 kN or lower, the impact severity would be significantly reduced.

Comparison with the previously discussed injury tolerance levels shows that the model predicts a significant safety benefit if poles are modified to breakaway. However, the TRB impulse criterion would appear difficult to meet. Moreover, considering the results depicted in Figure 1.8, and the world-wide trend to smaller, lighter vehicles, alternative impact attenuation solutions may have to be sought in the years to come.

Increasing vehicle crush stiffness results in decreased velocity change (Figure 1.9). Peak deceleration levels rise only marginally. The lower the crush stiffness, the longer it takes for the force between the vehicle and the pole to build up to $F_s$, and therefore the greater the velocity change.
Figure 1.7 Effect of pole shear force on crash severity

Figure 1.8 Effect of vehicle mass on crash severity

Figure 1.9 Effect of vehicle crush stiffness on crash severity
1.3.3 Discussion

Subsequent to the foregoing analysis, a paper by Owings and Cantor (1976) was found to have adopted a similar approach in the analysis of breakaway luminaire supports. The present analysis differs from Owings' and Cantor's in three respects:

(a) Owings and Cantor modelled the shearing phase of the pole as occurring over a finite time interval. In the simplified analysis presented here, shearing was assumed to occur instantaneously.

(b) The utility pole model includes a pin joint at the top of the pole to model the restraining effect of the conductor cables, while Owings' and Cantor's luminaire support was free to rotate and translate once the base had sheared.

(c) The final equations with which Owings and Cantor predict vehicle impulse as a function of impact speed, vehicle crush stiffness and pole characteristics include a number of approximations, some empirically based, which are claimed to be valid over an 'acceptable' range of impact velocities. These approximations result in a simplified calculation procedure for resultant vehicle impulse.

If the appropriate modifications are made to the Owings and Cantor equations to allow for the pin joint at the top of the pole, the predicted vehicle velocity change for an impact with the baseline modified wooden utility pole is 10.7 m/s compared with 7.8 m/s predicted by the present analysis.

1.4 SUMMARY OF FINDINGS

(a) The tests performed by Wolfe et al. (1974) and Labra (1977) demonstrate the feasibility of the breakaway concept for cable-supporting timber poles. The nature of these preliminary tests is such, however, that extrapolation of the results to Australian conditions would not be valid.

(b) A review of published injury tolerances to vehicle deceleration and velocity change reveals that limits are not well
established (Appendix C). However, for the purposes of design evaluation the TRB impulse criterion of 4890Ns has been chosen as the lower or no-injury limit, with the upper or serious-injury limits being chosen as 12 m/s velocity change or 25g average deceleration (Chi, 1976).

(c) A mathematical simulation of a vehicle-pole impact indicates the feasibility of a breakaway concept for the Australian pole timber and vehicle modelled.
CHAPTER 2

A SCALE MODEL FACILITY FOR THE INVESTIGATION OF THE BREAKAWAY CONCEPT FOR CABLE-SUPPORTING UTILITY POLES

2.1 INTRODUCTION

As the mathematical model results indicated the feasibility of the breakaway concept, it was decided to undertake a program of scale model tests to investigate the problem further. Scale model tests were chosen in preference to full scale tests because:

(a) They should provide an accurate simulation of full-scale tests, providing the laws of similitude provided by dimensional analysis are adhered to.

(b) They allow the investigation of the feasibility of the idea, as well as the elimination of unimportant parameters, for a cost which is an order of magnitude less than for full-scale tests.

The use of scale models in the evaluation of design features has a long history. Their use in aerodynamic studies in wind tunnels, and in hydrodynamic studies of hull shapes is well known.

The use of models in crash research was first reported in 1954. Ayre and Abrams (1954), and Ayre (1955, 1956) performed model tests on post-cable type crash barriers. Scale model barrier impact tests have also been carried out by Jurkat and Starret (1971), and Helm, Free, Warner and Fransden (1976). High speed films of the model tests were compared with full-scale tests, and showed good correlation. A similar comparison between model and full-scale tests of drum-type barriers was made by Fay and Wittrock (1971). They also achieved good results. Scale model testing was also used to develop energy-absorbing bumper bars in the Experimental Safety Vehicle program by Holmes and Colton (1974). Holmes and Sliter (1974) examined the utility and cost-effectiveness of scale model experiments for crashworthiness research. They demonstrated the ability of properly constructed and scaled models to reproduce the
response of a wide variety of vehicle components. They concluded that 'scaled experiments increase the flexibility, reduce the cost, and hasten the completion of a program'. These previous investigations of the use of scale models in crash testing have clearly established the usefulness and validity of the technique.

Emori (1973) carried out a scale model testing program of particular relevance to the present problem. From his study of automobile collisions with breakaway sign structures and breakaway luminaire poles, Emori concluded: 'The agreement in automobile-collision tests of model and prototype shows that scale models can substitute and screen out many preliminary full-scale tests in design of road-side obstacles for safety. Only those designated by model tests as crucial for the final proof must be tested by full scales.' (Emphasis added by present authors.)

2.2 VEHICLE-POLE-CABLE SYSTEMS MODELLLED

Two prototype pole-cable systems and a timber luminaire pole were modelled. The full-scale layouts of these systems are shown in Figures 2.1 to 2.3. Figure 2.1 details the configuration most commonly found on arterial and sub-arterial roads in metropolitan Melbourne. It consists of three high voltage lines and five low voltage lines, the low voltage lines being supported by the lower of two cross-arms. The pole material is ironbark.*

A second common pole-cable configuration is shown in Figure 2.2. It is made up of a single cross-arm attached to a messmate pole and supports five low voltage lines.

The messmate timber luminaire pole modelled is shown in Figure 2.3.

The vehicle modelled was a standard six-cylinder sedan. The crushable nose on the car was the subject of considerable development work which is discussed in Section 2.5.

* Ironbark is classified as a regal species timber and is a member of the class 1 durability group. Messmate is a member of the class 3 durability group (Pearson, Kloot and Boyd, 1962).
CROSSARM HV10
MATERIAL: IRONBARK
(HIGH VOLTAGE CONDUCTORS)

CROSSARM LV31
MATERIAL: IRONBARK
(LOW VOLTAGE CONDUCTORS)

POLE: 12m /&N CLASS 1
MATERIAL: IRONBARK

All dimensions in mm

Figure 2.1. High and low voltage conductor configuration.
CROSSARM: LV31\(^{(1)}\)
MATERIAL: IRONBARK
(Low voltage conductors)

POLE: 11 m/8 kN CLASS 3
MATERIAL: MESSMATE

All dimensions in mm.

Figure 2.2. Low voltage conductor configuration.
MASS OF LIGHT ASSY. = 25.6 kg
MASS OF LIGHT ARM = 25.8 kg

POLE MATERIAL: MESSMATE

ROADWAY

GROUND

All dimensions in mm.

Figure 2.3. Luminaire configuration.
2.3 DIMENSIONAL ANALYSIS OF THE SYSTEM

The first step in the construction of a scale model is the selection of the independent variables thought to be the major determinants of system performance. It is inevitable that some second-order influences will not be modelled exactly; the task is to ensure that the first-order effects are.

In the vehicle/pole/conductor system the most important influences will be the vehicle impact speed, the inertias and flexibilities of system components, the force levels at which components fail, and gravity. Dynamic similitude between model and prototype requires that all corresponding forces $F$ (e.g., failure loads or weights) be related by the same scale factor, $S_F = F_{\text{model}}/F_{\text{prototype}}$. Since the scale factor for gravity is necessarily unity ($S_g = 1$) for practical earthbound experiments, forces $F$ and masses $M$ must scale in the same way; i.e., $S_F = S_M$.

Here a choice may be made. If prototype materials are used in the model in a geometrically similar arrangement, the scale factors for failure stresses $\sigma$ and elastic constants $E$ will be approximately unity (depending only on size effects on the material constitutive relations); i.e., $S_\sigma = 1$, $S_E = 1$. With this choice, failure loads will scale the same way as areas of material; i.e., $S_F = S_L^2$, where $S_L = L_{\text{m}}/L_{\text{p}}$ is the length scale factor. The self-weight of components will then scale as $S_L^3$, so that to maintain $S_F = S_M$, component weights must be artificially increased by a factor of approximately $1/S_L$, without affecting the deformation behaviour of the material. This was the approach taken in the present study.

An alternative approach is to use different materials and to sacrifice geometric similarity in such a way as to maintain $S_\sigma = S_E = S_D S_L$, where $S_D$ is the scale factor for density. This was rejected as impractical and undesirable for the present study, in which the material properties (of the poles and cables especially) were of some importance.

Table 2.1 lists the variables chosen for dimensional analysis. The pole is characterized by its height $h$ and a representative...
<table>
<thead>
<tr>
<th>Variable</th>
<th>Variable Description</th>
<th>Dimensions</th>
</tr>
</thead>
<tbody>
<tr>
<td>$M_p$</td>
<td>Mass of pole</td>
<td>$M$</td>
</tr>
<tr>
<td>$r$</td>
<td>Radius of gyration of pole</td>
<td>$L$</td>
</tr>
<tr>
<td>$h$</td>
<td>Height of pole</td>
<td>$L$</td>
</tr>
<tr>
<td>$E_p$</td>
<td>Modulus of elasticity of pole material</td>
<td>$ML^{-1}T^{-2}$</td>
</tr>
<tr>
<td>$\sigma_p$</td>
<td>Ultimate stress-pole</td>
<td>$ML^{-1}T^{-2}$</td>
</tr>
<tr>
<td>$A_p$</td>
<td>Pole cross-sectional area</td>
<td>$L^2$</td>
</tr>
<tr>
<td>$I_p$</td>
<td>Second moment of area of pole cross-section</td>
<td>$L^4$</td>
</tr>
<tr>
<td>$M$</td>
<td>Mass of car</td>
<td>$N$</td>
</tr>
<tr>
<td>$V$</td>
<td>Velocity of car</td>
<td>$LT^{-1}$</td>
</tr>
<tr>
<td>$k$</td>
<td>Crush stiffness of car</td>
<td>$ML^{-2}$</td>
</tr>
<tr>
<td>$e$</td>
<td>Impact coefficient of restitution</td>
<td>$-$</td>
</tr>
<tr>
<td>$L$</td>
<td>Span of cables</td>
<td>$L$</td>
</tr>
<tr>
<td>$S$</td>
<td>Sag of cables</td>
<td>$L$</td>
</tr>
<tr>
<td>$T$</td>
<td>Ultimate force - cables</td>
<td>$MLT^{-2}$</td>
</tr>
<tr>
<td>$\mu$</td>
<td>Cable extensional rigidity</td>
<td>$MLT^{-2}$</td>
</tr>
<tr>
<td>$\mu$</td>
<td>Mass per unit length of cables</td>
<td>$ML^{-1}$</td>
</tr>
<tr>
<td>$g$</td>
<td>Gravitational acceleration</td>
<td>$LT^{-2}$</td>
</tr>
</tbody>
</table>
cross-sectional area, \( A_p \), its mass \( M_p \) and inertia distribution (represented by a radius of gyration \( r \)). To allow for pole flexure, a representative second moment of area \( I_p \) is specified, together with the modulus of elasticity \( E_p \). The strength of the pole is represented by an ultimate fibre stress \( q_p \). For the cable, the main properties to be accounted for are its length \( L \) and sag \( S \), its mass per unit length \( \mu \) and its extensibility, represented by the product of its modulus of elasticity \( E_c \) and cross-sectional area \( A_c \). Because the stiffness of the cable in bending is assumed to be negligible, only the product \( E_c A_c \) is specified and the cable diameter is not constrained to scale in geometric proportion. The strength of the cable is represented by the ultimate tensile load \( T \). For the car, the variables of importance in the collision are its mass \( M \), speed \( V \) and a measure of the crush 'stiffness', \( k \). The coefficient of restitution \( e \) is used to represent the degree of elastic recovery following deformation in the collision.

The seventeen variables in Table 2.1 may be arranged into 14 independent, dimensionless \( \Pi \) products, the values of which determine the nature of the system behaviour. To achieve similitude between model and prototype, the model parameters must be chosen so that the model \( \Pi \) products are equal to the prototype \( \Pi \) products.

Choosing the variables \( \mu \), \( L \) and \( g \) to represent, between them, the dimensions of mass, length and time, the following fourteen \( \Pi \) products may be formed:

\[
\begin{align*}
\Pi_1 &= \frac{M_p}{\mu L} \\
\Pi_2 &= \frac{r}{L} \\
\Pi_3 &= \frac{h}{L} \\
\Pi_4 &= \frac{E_p A_p}{\mu g L} \\
\Pi_5 &= \frac{q_p A_p}{\mu g L} \\
\Pi_6 &= \frac{A_p}{L^2} \\
\Pi_7 &= \frac{I_p}{L^4} \\
\Pi_8 &= \frac{M}{\mu L} \\
\Pi_9 &= \frac{v^2}{qL} \\
\Pi_{10} &= \frac{k}{\mu g} \\
\Pi_{11} &= e \\
\Pi_{12} &= \frac{E_c A_c}{\mu g L} \\
\Pi_{13} &= \frac{T}{\mu g L} \\
\Pi_{14} &= \frac{E_c A_c}{\mu g L}
\end{align*}
\]
Geometric similarity of model and prototype poles and cable span/sag arrangements will ensure equality of $\Pi_2$, $\Pi_3$, $\Pi_6$, $\Pi_7$ and $\Pi_{12}$ in both systems. Froude number similarity (equality of $\Pi_9$) will ensure that accelerations are properly scaled in relation to gravity. Selection of material properties must be made so as to satisfy the requirements of equality of the remaining $\Pi$s as closely as is possible.

2.4 CALCULATION OF THE MODEL SCALE FACTORS

As indicated previously, scale model testing in which material stress-strain properties are important generally require that the prototype materials be used in the construction of the model. In the present system material properties are particularly important for the shearing of the pole, and the response of the cables. Therefore, it was decided that these two components should be constructed from prototype materials.

The prototype cables are 19/3.25 All Aluminium Conductor Cable. The smallest diameter of aluminium wire available for use as the model cable in fact determined the length scale factor (although not simply by the ratio of cable diameters). Because of the difference between the mechanical behaviour of stranded and non-stranded cable, the modulus of elasticity $E_C$ and ultimate stress $\sigma_C$ of the single strand model cable were somewhat different from those for the 19-strand prototype cable. From Australian Standard A.S. 1531 'Aluminium Conductors for Overhead Transmission' the scale factors were found to be:

$$S_{E_C} = 1.21$$

$$S_{\sigma_C} = 1.12$$

Similarly, there is a size effect on the properties of the pole timber. From Boyd (1968), the scale factors for modulus of elasticity $E_P$ and ultimate fibre stress $\sigma_P$ in bending, determined for small clear specimens of both messmate and ironbark, are approximately:
\[
S_{E_p} = 0.88 \\
S_{\sigma_p} = 0.96
\]

It is noted that the timber used in the model poles came from the same districts as the full-scale poles and was provided by the SECV. Discussions with the Commonwealth Scientific and Industrial Research Organization (CSIRO) revealed that for the length scale factor being used in the model, grain size effects in the timber would be negligible.

From \( \Pi_{13} \) and \( \Pi_{14} \), an alternative \( \Pi \) product may be formed:

\[
\frac{\Pi_{14}}{\Pi_{13}} = \frac{T}{E A} = \frac{\sigma_c}{E_c}
\]

Hence, exact similitude requires:

\[
\frac{\sigma_c}{S_{E_c}} = 1
\]

whereas for the model cable:

\[
\frac{\sigma_c}{S_{E_c}} = \frac{1.12}{1.21} = 0.93
\]

Thus the requirement of equation (2.1) cannot be met exactly. However, \( S_{\sigma_c} \) and \( S_{E_c} \) differ by only seven percent. As it was thought that modelling ultimate stress in the cable was more important than modelling the cable stiffness exactly, it was decided to assume:

\[
S_{\sigma_c} = S_{E_c} = 1.12
\] (2.2)

Similarly, for the pole, \( \Pi_{4} \) and \( \Pi_{5} \) show that exact similitude requires \( \frac{S_{\sigma_p}}{S_{E_p}} = 1 \). Because of size effects in the timber, however,

\[
\frac{S_{\sigma_p}}{S_{E_p}} = \frac{0.96}{0.88} = 1.09
\]
in the present model; i.e., a nine percent error. Again, because precise modelling of the strength of the pole is more important than representing its deformations exactly, it was assumed that

\[ S_{\text{op}} = S_{\text{Ep}} = 0.96 \]  \hspace{1cm} (2.3)

The length scale factor can now be calculated. From \( \Pi_5 \) and \( \Pi_{14} \),

\[ S_{EC} S_{AC} \]

But, from \( \Pi_6 \),

\[ = S_L^2 \]

Hence,

\[ S_L = \sqrt{\frac{S_{EC} S_{AC}}{S_{\text{op}}}} \]  \hspace{1cm} (2.4)

Now \((A_C)\) prot. = 157.60 mm\(^2\)

\((A_C)\) mod. = 2.489 mm\(^2\)

Thus, from \( (2.2 - (2.4), \]

\[ S_L = 0.1355 = 1/7.38 \]

Thus, the use of a single strand of aluminium wire to model the cables results in a length scale factor of 1/7.38. This is high in comparison with previous studies in which the scale factors ranged typically from 1/12 to 1/25. Larger models have the disadvantages of increased cost and space requirements, but have the advantage that the detail can be represented more accurately.

As previously discussed, a consequence of constructing the model from prototype materials is that self-masses scale as \( S_L^3 \). Similitude, however, requires that masses scale approximately as \( S_L^2 \). For example, \( \Pi_5 \), \( \Pi_6 \) and \( \Pi_8 \) require:

\[ S_M = S_{\text{op}} S_L^2 = 0.96 S_L^2 \]  \hspace{1cm} (2.5)
That is, the model mass must be increased by a factor of \( 0.96/S_L \) over its self-mass.

To achieve this, lead weights were added to the cables and to the target pole and cross-arms in such a way as to minimize interference with the flexural behaviour, as shown in Figure 2.15. The lead rings on the target pole were adjusted and located in such a way that there was only a 2% alteration to the radius of gyration of the pole. The lead weights on the cable were placed every 8 cm over a span of 4.74 m. Thus the cable dynamics should be simulated accurately except for the very high frequency modes.

A further check of the validity of the cable models was provided by wave sagging. In practice, large cable spans have their sag set by measuring the time it takes a disturbance to travel up and down the span. The sag is obtained from plots of wave travel time against span for given sags and cables. Such a test was carried out on the model cables, and the scaled-up wave travel time for the measured cable sag was in close agreement with that predicted from the full-scale charts.

For ease of manufacture all the model poles were octagonally dressed, and were initially made the same size. For the low voltage and luminaire configurations in which mate poles were used, the poles were shortened by removing the required amount from the top of the pole. These poles consequently had slightly over standard girths. This was taken into account in the pole strength and modification calculations, but inertias and stiffnesses would have been slightly high.

Having established scale factors for length and mass, the remaining primary scale factor is for time, \( t \). From (2.9):

\[
S_V = \frac{S_L}{S_t} = S_L^{1/2}
\]

Hence,

\[
S_t = \sqrt{S_L}
\]

In summary, the scale factors for various quantities of interest are as follows:
<table>
<thead>
<tr>
<th>Variable</th>
<th>Scale Factor</th>
</tr>
</thead>
<tbody>
<tr>
<td>X</td>
<td>$S_x = \frac{x_m}{x_p}$</td>
</tr>
<tr>
<td>Length</td>
<td>$S_L = 0.1355$</td>
</tr>
<tr>
<td>Mass</td>
<td>$0.96 S_L^2 = 0.0176$</td>
</tr>
<tr>
<td>Time</td>
<td>$S_L^4 = 0.3681$</td>
</tr>
<tr>
<td>Velocity</td>
<td>$S_L^4 = 0.3681$</td>
</tr>
<tr>
<td>Acceleration</td>
<td>$S_L^6 = 1$</td>
</tr>
<tr>
<td>Force</td>
<td>$0.96 S_L^2 = 0.0176$</td>
</tr>
<tr>
<td>Stress</td>
<td>$0.96 S_L^6 = 0.96$</td>
</tr>
</tbody>
</table>

Table 2.2 shows the resultant model and full-scale dimensions and masses side by side.

Due to an arithmetic calculation error the design masses of the model components listed in this table are one percent low. This error is insignificant in comparison with manufacturing tolerances and the uncertainty of scale factors for material properties.

2.5 MODELLING OF VEHICLE FRONTAL CRUSH CHARACTERISTICS

The frontal impact stiffness of the model car had to be provided by a device that would be re-usable or easily replaced, and produce the correct dynamic force-displacement response. The prototype force-displacement curve was derived from McHenry et al., (1967) in which both static and dynamic test results were obtained for two different vehicles. The dynamic tests consisted of dropping the vehicles onto a strain-gauged pole which was rigidly supported across a pit. This was the only report found which gave force-displacement data for pole impacts.

It was thought that the simplest solution might be provided by a replaceable 'nose' rather than a mechanical device. As a first step, static compression tests were carried out on a variety of materials and shapes including polystyrene and polyurethane foams, various metal sections and metal sheets.
TABLE 2.2

PROTOTYPE AND SCALE MODEL SPECIFICATIONS

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Model</th>
<th>Prototype</th>
</tr>
</thead>
<tbody>
<tr>
<td><strong>Ironbark Poles</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Height above ground (m)</td>
<td>1.33</td>
<td>9.80</td>
</tr>
<tr>
<td>Base diameter (mm)</td>
<td>42</td>
<td>309</td>
</tr>
<tr>
<td>Top diameter (mm)</td>
<td>26</td>
<td>194</td>
</tr>
<tr>
<td>Mass (kg)</td>
<td>7.74</td>
<td>444.0</td>
</tr>
<tr>
<td><strong>Messmate Poles</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Height above ground (m)</td>
<td>1.17</td>
<td>8.60</td>
</tr>
<tr>
<td>Base diameter (mm)</td>
<td>42</td>
<td>309</td>
</tr>
<tr>
<td>Top diameter (mm)</td>
<td>28</td>
<td>208</td>
</tr>
<tr>
<td>Mass (kg)</td>
<td>5.18</td>
<td>297</td>
</tr>
<tr>
<td><strong>Cable</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>X-section area (m²)</td>
<td>2.489 x 10^-6</td>
<td>1.576 x 10^-6</td>
</tr>
<tr>
<td>Mass/length (kg/m)</td>
<td>0.0557</td>
<td>0.433</td>
</tr>
<tr>
<td>Material</td>
<td>Aluminium</td>
<td>Aluminium</td>
</tr>
<tr>
<td><strong>Vehicle</strong></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Length (m)</td>
<td>0.656</td>
<td>4.84</td>
</tr>
<tr>
<td>Width (m)</td>
<td>0.255</td>
<td>1.88</td>
</tr>
<tr>
<td>Height (m)</td>
<td>0.186</td>
<td>1.37</td>
</tr>
<tr>
<td>Mass (kg)</td>
<td>23.42</td>
<td>1343</td>
</tr>
</tbody>
</table>
Figure 2.4. Scale model vehicle deformable nose
The static tests resulted in the prototype nose shown in Figure 2.4. It consisted of four aluminium box sections of varying depth and wall thickness which ran in tracks in the front section of the vehicle. Figure 2.5 shows the front section of the vehicle with an undamaged nose in place.

To investigate the dynamic characteristics of the nose, a series of impacts with a solid steel pole were carried out. The resulting nose deformation from one such test is shown in Figure 2.6.

Vehicle deceleration was measured with a Bruel and Kjaer accelerometer type 4366 and conditioning amplifier type 2635. The signal was recorded and integrated twice using a Tektronix WP 1100 Digital Processing Oscilloscope System. Vehicle deceleration was then plotted digitally against displacement to give the dynamic force-deflection curve for the nose.

A vehicle deceleration record that resulted from impact with a rigid pole at a velocity of 6.08 m/s (equivalent to 16.52 m/s full scale) is presented in Figure 2.7. The resultant 'force-deflection curve' is shown in Figure 2.8*. Both these figures have been taken directly from the digital plotter and have not been rescaled or replotted.

It can be seen from Figure 2.9, that the dynamic test results follow the crush characteristic obtained by McHenry et al., (1967) very closely. For the materials and sections used in the model, the strain rate dependence appears to be minimal for the range of impact speeds tested.

The results obtained with the model nose were highly repeatable and consistent for both low and high speed impacts.

* Recall that the vehicle acceleration \( \ddot{x} \) is related to the force between the nose and the pole by \( \ddot{x} = -F/M \), where \( M \) is the vehicle mass.
Figure 2.5. Crush 'nose' prior to impact

Figure 2.6. Crush 'nose' after impact with solid steel pole
Figure 2.7. Vehicle deceleration resulting from solid steel pole impact
\[ V = 6.08 \text{ m/s} \]

Figure 2.8. Dynamic force-displacement curve for scale model vehicle
Figure 2.9. Comparison of static and dynamic model force-deflection results with data of McHenry et al., 1967)
2.6 POLE-CABLE SYSTEM MATERIAL TESTS

2.6.1 Cable Ties

One of the most important elements in the pole-cable system is the method of attachment of the conductor cables to the pole cross-arms. This is important because, for the two-cross-arm configuration, the cable forces must provide the bending moment necessary to fracture the pole at the top weakened zone, just below the cross-arms. The cable ties are also required to support the top section of the pole and cross-arms after impact.

On the full-scale pole the conductor cables are tied to ceramic insulators with aluminium wire. The insulators are attached to the cross-arms by steel insulator pins.

In order that the properties of the cable attachment could be modelled accurately, a series of tests were conducted to establish the characteristics of the full-scale system. Two different types of insulators and pins were tested: An 11 kV high-voltage insulator and pin, and a low-voltage insulator and pin. The insulators were tied onto 1 m lengths of 19/3.25 AAC* cable in accordance with SECV specifications and were subjected to loads at angles to, and perpendicular to, the insulator pin.

Figure 2.10 shows an 11 kV insulator and pin installed in the testing machine, with load yet to be applied. The test depicted is an oblique-load test in which the portion of the cable above the insulator goes into tension. A parallel load test (perpendicular to the insulator pin) is shown in Figure 2.11.

The test results are contained in Table 2.3.

As a result of the tests, and on the advice of the SECV that the cable ties, rather than the insulators or insulator pins, usually fail in pole impacts, scale models of the insulators and pins were fashioned from brass machine screws. A groove was filed in the screws to take the model cable and the cable was tied to the screw using 10 amp fuse wire. The method of tying was

* All Aluminium Conductor.
Figure 2.10. LV insulator and pin being subjected to an oblique load

Figure 2.11. LV insulator and pin during parallel load test
### TABLE 2.3

**PROTOTYPE CABLE TIE TESTS**

<table>
<thead>
<tr>
<th>Insulator</th>
<th>Test</th>
<th>Failure Load (N)</th>
<th>Failure Mode</th>
</tr>
</thead>
<tbody>
<tr>
<td>LV</td>
<td>Oblique</td>
<td>4495 - 5205</td>
<td>Ties, pin and insulator all failed at or near this load.</td>
</tr>
<tr>
<td>LV</td>
<td>Parallel</td>
<td>4495 - 5295</td>
<td>First tie fails on the opposite side of the insulator to the load.</td>
</tr>
<tr>
<td>HV</td>
<td>Oblique</td>
<td>4490 - 5250</td>
<td>Ties only failed, with a small amount of pin deformation.</td>
</tr>
<tr>
<td>HV</td>
<td>Parallel</td>
<td>4045 - 5295</td>
<td>First tie to fail was on the opposite side of the insulator to the load.</td>
</tr>
</tbody>
</table>

### TABLE 2.4

**SCALE MODEL CABLE TIE STRENGTH TESTS**(1)

<table>
<thead>
<tr>
<th>Tie Wire</th>
<th>Parallel Test</th>
<th>Oblique Test</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Failure Load</td>
<td>Std. Dev'n.</td>
</tr>
<tr>
<td></td>
<td>(N)</td>
<td></td>
</tr>
<tr>
<td>10 amp. fuse wire</td>
<td>87.7 (5029)</td>
<td>5.1</td>
</tr>
<tr>
<td>15 amp. fuse wire</td>
<td>162.4 (9312)</td>
<td>1.4</td>
</tr>
<tr>
<td>20 amp. fuse wire</td>
<td>242.5 (13905)</td>
<td>29.2</td>
</tr>
</tbody>
</table>

(1) Full scale equivalent failure load is shown in brackets.
exactly the same as in the prototype. It was found that in both oblique and parallel pull tests the model failure load scaled to the prototype failure load well. The mode of failure of the model was identical to that of the prototype.

A range of stronger scale model tie wires were also tested, with the results shown in Table 2.4.

A new, stronger cable-tying system, still under development by the SECV, was also tested. It was found that, for the HV insulators and pins, the insulators rather than the ties failed in the parallel load test at a load of about 6000 N. The new system did not perform as well in the oblique pull test, and tended to pull off the insulator at 1750 N, with no damage to the tie, cable or insulator. LV cable insulator systems were not tested with the new system as the insulators failed with the lower strength conventional ties.

2.6.2 Conductor Cable

To check the comparative ultimate strengths of the prototype and model cables, specimens of both were tested in tension until failure occurred.

Figure 2.12 shows the results of both tests and it can be seen that the model and the prototype are in very close agreement. It is apparent that the single strand model cable provides a satisfactory simulation of the full-scale stranded cable. The small difference in Young's modulus was anticipated in Section 2.4.

2.6.3 Pole Timber Tests

The strength of the pole timber is of fundamental importance to the calculation of the amount of material available for removal both at the base of the pole and near the cross-arms. It is from the ultimate failure stress that safe working stresses are determined, and hence the section modulus required to withstand service loads.

Boyd (1961-1968) carried out static bending tests on a variety of timber species poles at full scale as well as on matched small clear specimens obtained from the full scale poles. He found that
Figure 2.12. Comparison of the tensile tests of prototype and model cable
the clear specimens gave good estimates of the full-scale behaviour (but with a scale factor of about 0.96 - see Section 2.3).

Bending tests were carried out in the present work to allow a comparison of the timber strengths of the scale model poles with the results reported by Boyd. It was also necessary to establish the strength of the material in each pole tested, so that any variation of breakaway performance could be related to the timber strength.

The relevance of static bending tests to dynamic shear properties is questionable, although it may be argued that a pole demonstrating a higher strength is likely also to exhibit a higher dynamic shear strength.

The test specimens were obtained from off-cuts of the blocks from which the model poles were cut. The specimens were tested as a centrally-loaded, simply-supported beam. The tests followed B.S.373* as closely as possible.

B.S.373 recommends that the specimen should be 2 cm by 2 cm by 30 cm, and that the span be 28 cm. It was not always possible to obtain specimens of 2 cm by 2 cm, although the nearest specimens to that size were used. Five specimens from each pole were tested and the results averaged.

The results are shown in Table 2.5, along with those from Boyd (1961, 1968). It can be seen that the bending strengths of the scale model timbers are greater than the means reported by Boyd.

TABLE 2.5

POLE TIMBER MECHANICAL PROPERTIES

<table>
<thead>
<tr>
<th>Timber Species</th>
<th>( \sigma_{\text{ult.}} ) (measured) MPa</th>
<th>( \sigma_{\text{ult.}} ) (Boyd)</th>
<th>( \sigma_{w} ) (Boyd)</th>
<th>( \sigma_{w} ) (SECV)</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Mean (4) Std. Dev'n.</td>
<td>MPa (1)</td>
<td>MPa (2)</td>
<td>MPa (3)</td>
</tr>
<tr>
<td>Ironbark</td>
<td>169.8 31.6</td>
<td>123.4</td>
<td>66.2</td>
<td>60</td>
</tr>
<tr>
<td>Messmate Stringybark</td>
<td>96.5   9.9</td>
<td>68.4</td>
<td>45.1</td>
<td>48</td>
</tr>
</tbody>
</table>

(1) Figures quoted refer to small clear green timber specimens tested in accordance with B.S. 373.
(4) Results of bending tests on air-dry small clear timber specimens, tested in accordance with B.S.373.
This is most probably due to the fact that the specimens tested by Boyd were green (high moisture content), whereas those tested in the present study were air-dry. Pearson, Kloot and Boyd (1962) and Boyd (1962) report that the modulus of rupture of unchecked timber rises with decreasing moisture content. For example, Boyd reported that an increase above the green pole working stress for air-dry messmate stringybark of 30% could be regarded as conservative. For grey ironbark larger increases could be observed. Table 2.5 clearly demonstrates this phenomenon, with the air-dry specimens having a mean modulus of rupture around 40% higher than the values reported by Boyd for green specimens.

From the point of view of pole modifications near ground level, it would be reasonable to assume a modulus of rupture nearer to the green or saturated fibre value because of probable high moisture content and degradation of the sapwood. The modulus of rupture for the upper modification zone on the other hand would be expected to be higher than the green timber value because of a lower timber moisture content than near ground level.

2.7 CALCULATION OF ALLOWABLE POLE SECTION-MODULUS REDUCTION

2.7.1 Rated Pole Strength

The strength rating of poles used by the State Electricity Commission of Victoria (SECV) relates to the transverse load (e.g., 3 kN, 6kN, etc.) which can be supported at a point on the pole 300 mm below the top without exceeding an allowable working stress in bending. The required section modulus at any point along the pole can then be determined using standard bending theory.
Typically, the transverse loads which poles must sustain in service result from wind loads on the conductors and on the pole itself. These loads are classified as short duration loads. Long duration loads arise from unbalanced cable tensions, such as at termination or deviation poles. Where such loads are present the working stress allowed by the SECV is halved. Alternatively, the out-of-balance loads can be negated by installing a guy wire.

Appendix E contains extracts from the Overhead Line Manual (SECV, 1978) which allow the calculation of pole loading due to wind or out-of-balance cable tensions.

For example, the two-crossarm pole represented in Figure 2.1, which supports eight 19/3.25 AAC conductors, has to be able to sustain a short duration load of 3.1 kN due to wind loading on both the conductors and pole. Different cable sizes, spans, pole configurations, etc. would obviously apply different loads to the pole. When considering the modification of a pole by material removal, the required pole strength should be determined for each individual case.

2.7.2 Allowable Pole Working Stress

The derivation of allowable working stresses for timber poles is described by Boyd (1962). The 10% lower probability limit of the modulus of rupture (to account for timber strength variation) is modified by a series of factors which take account of:

(a) Deterioration during service life.
(b) Seasoning de-grade factor.
(c) Adjustment for the position of maximum stress being different from that in the beam test.
(d) Duration of loading.
(e) Provision for lack of precision.
The working stresses in bending recommended by Boyd for short duration loads are shown in Table 2.5. Short duration loads refer typically to wind loads on the conductors and pole. The working stresses adopted by the State Electricity Commission of Victoria (SECV) for this type of loading are also shown in Table 2.5. For the purposes of designing the scale model breakaway mechanisms, however, Boyd's working stresses were used.

2.7.3 Section Modulus Required for Full Scale Strength Specifications

For a given pole loading and recommended working stress in bending, the required section modulus at any station along the pole can be determined. The amount of material that can be removed at any station can then also be determined. An example calculation to determine the allowable hole sizes for an 8 kN, 9.8 m ironbark pole modified according to the crossed-hole scheme of Wolfe et al (1974) follows.

The strength rating of poles used by the SECV relates to the transverse load \( P \) which can be supported at a point on the pole 300 mm from the top. The required section modulus \( Z \) at a distance \( h \) below the load application point is given by

\[
Z = \frac{Ph}{\sigma_w}
\]

where \( \sigma_w \) is the working stress in bending.

For the subject pole:

\[
\begin{align*}
\ h &= 9.35 \text{ m} \\
\ P &= 8 \text{ kN} \\
\ \sigma_w &= 66.19 \text{ Mpa}
\end{align*}
\]
Figure 2.13. Cross-section of modified pole
The section modulus required at a point 10.15 m above ground level is then

\[ Z = \frac{8000 \times 9.35}{66.19 \times 10^6} \]

\[ Z = 1.13 \times 10^{-3} \text{ m}^3 \]

The section modulus available at the same location for an octagonally-dressed pole is:

\[ Z_A = 0.109 D^3 \]

where \( D \) is measured across the flats (see Figure 2.13).

For \( D = 0.291 \text{ m} \)

\[ Z_A = 2.69 \times 10^{-3} \text{ m}^3 \]

Clearly, material removal is possible. For the modified section shown in Figure 2.13, the centroidal second moment of area \( I \) is:

\[ I = 0.55 D^4 - DD^3/12 - dD^3/12 + d^4/12 = ZD/2 \]

Hence,

\[ (d/D)^4 - (d/D)^3 - (d/D) - (6Z/D^3) = 0.66 \]

For \( Z = 1.13 \times 10^{-3} \text{ m}^3 \)

\[ D = 0.291 \text{ m} \]

the solution of the equation above is:

\[ d/D = 0.356 \]

That is:

\[ d_{\text{MAX}} = 104 \text{ mm} \ (4.1") \]

This calculation was carried out for each scale model pole modification.
Figure 2.14. Plan of the experimental layout.
2.8 EXPERIMENTAL SET-UP

A line of 1:7.38 scale model poles was set up in a laboratory. A plan of the experimental layout is shown in Figure 2.14, and a general view of the set-up is shown in Figure 2.15.

The pole-cable system shown in Figure 2.15 consisted of eight cables (representing five low voltage and three high voltage lines) and five poles. The target pole was flanked by identical wooden poles, with the cables being terminated on adjustable turnbuckles mounted on steel poles.

The model poles were cut from the timber provided by the SECV using a band saw and jig which produced octagonally-dressed, tapered poles.

The model vehicle was launched by a spring-actuated plunger at an angle to the pole line which was variable between 0° and 50°.

The vehicle deceleration was obtained from an accelerometer mounted on the vehicle. The signal was recorded, and integrated to give velocity change using a digital processing oscilloscope system (detailed in Section 2.5). Impact velocity was determined from a twin light beam timing gate, placed just before the impact point. High-speed motion picture films of the tests were taken from two locations (see Figure 2.14). This allowed the post-impact trajectory of the pole and vehicle to be monitored as well as the motion of the conductor cables.

Cable tensions were recorded for some of the tests involving the HV and LV cable configuration (two cross-arms) and for the majority of the LV cable configuration (one cross-arm) tests.

The tension transducers were fabricated from a high strength aluminium alloy (5083), and consisted of a small beam which flexed
Figure 2.15. General view of experimental layout.
Figure 2.16. Cable tension transducer.

Figure 2.17. Shielded cable tension transducers in position prior to the conduct of a
when the cable was in tension. A four-arm strain gauge bridge on
the beam produced an output proportional to the cable tension.
The signal outputs were fed through signal conditioning amplifiers
and were recorded on a seven-track FM reel-to-reel tape recorder
(Sony DFR-3715).

Figure 2.16 shows the details of the transducer, and Figure
2.17 shows the shielded transducers in place prior to a test.
Note that the transducers were clamped to the conductors, thereby
eliminating any need to cut the conductors. Interference with
cable response was thought to be minimal, because of the small
size and weight of the transducers and their positioning close to
the cross-arms. The dynamic performance of the transducers proved
to be most satisfactory during dynamic calibration and the sub-
sequent test program.