

CRANFIELD UNIVERSITY

S. KOIDE

**THE EFFECTS OF JUNCTION MODIFICATIONS
ON SHARP-FIN-INDUCED GLANCING SHOCK
WAVE/TURBULENT BOUNDARY LAYER
INTERACTION**

College of Aeronautics

Ph.D Thesis

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COLLEGE OF AERONAUTICS

PhD THESIS

Academic Years 1991-3

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**THE EFFECTS OF JUNCTION MODIFICATIONS ON SHARP-FIN-
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TURBULENT BOUNDARY LAYER INTERACTION**

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JUNE 1994

Abstract

The effects of junction modifications on the glancing shock wave/turbulent boundary layer interaction generated by a sharp fin placed on the wall of a supersonic wind tunnel were examined experimentally at a Mach number of 2.46 and a Reynolds number of $2.59 \times 10^6/m$. The interactions between a turbulent boundary layer on the wall and shock-wave systems caused by a fin with a fillet and several fins with different strakes were examined individually in order to find an effective modification technique. The flow features obtained by oil flow visualization, surface pressure measurements and liquid crystal thermography were compared with the data from an unmodified fin to evaluate the effects of each modification. The comparisons indicated that a "strake-type" modification had a weakening effect on the interaction-induced separation. To understand the flowfield structures, three-dimensional surveys using laser-light-sheet flow visualization were employed with schlieren photography and oil flow visualization. In addition to these experimental observations, an Euler CFD solver was used to help understand the inviscid flow structures which play important roles in the interaction behaviour. Based on the data experimentally and numerically obtained, a method was proposed for predicting the junction shapes needed to prevent separation.

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Notation

H	: Height
L	: Length
Re	: Reynolds Number
P	: Static pressure
PR	: Static pressure ratio
X, Y, Z	: Coordinate system in directions parallel and normal to the model axis (see Fig.2-17)
X_s, Y_s	: Coordinate system along and normal to the unmodified inviscid shock direction (see Fig.2-17)
X_f	: A distance from the unmodified-model leading edge, defined in Fig.2-17
α	: Half-apex angle or angle of attack
β	: Shock wave angle
δ	: Boundary layer thickness defined by 99.5% of the external flow velocity
δ^*	: Boundary layer displacement thickness
ζ	: Detachment similarity parameter
θ	: Boundary layer momentum thickness
η	: Fin-shock propagation angle (see Fig.5-12)
λ	: Strake sweep angle (see Fig.5-1(a))
μ	: Mach angle = $\sin^{-1}(1/M)$
ξ	: The angle between the plane of symmetry and the face of the rhombic delta wing (see Fig.5-10)
π	: Ratio of circumference of circle to diameter
ρ	: Angle of the tail edge of the strake (see Fig.2-5) or static density

Subscripts

dry	: dried air condition
eq	: "equivalent" condition
fin	: Value related to the unmodified part of the model
imp	: Value related to the fin shock impingement point (see Fig.5-12)
IPS	: Condition to cause "incipient primary separation"
ISS	: Condition to cause "incipient secondary separation"
LEF	: Value related to the lower edge of the fin shock (see Fig.5-1)
moist	: Air condition containing water vapour
n	: Component normal to a shock wave
nle	: Component normal to a leading edge
ns	: Value related to the normal shock wave
os	: Value related to the oblique shock wave
R	: Condition at the point R (see Fig.5-15,5-19)
req	: Minimum size of the strake dimension needed to prevent separation on the wall
str	: Value related to the strake
∞	: Free stream condition
1	: Condition at region 1 (see Fig.5-1(b))

List of abbreviations

CFD	: Computational Fluid Dynamics
IPS	: Incipient Primary Separation
LC	: Liquid Crystal
LEF	: Lower Edge of the Fin shock
LES	: Lower Edge of the Strake shock
LFV	: Laser light sheet Flow Visualization
RDW	: Rhombic Delta Wing
SPF	: Straight Part of the Fin shock

CHAPTER 1 Introduction

1.1 General view of glancing shock wave/boundary layer interaction and previous investigations on the interaction

Interaction between a glancing shock wave and a turbulent boundary layer can occur at many points over the fuselage and in the intake of a high speed flight vehicle (see Fig.1-1, reproduced from Ref.1). In the intake, several shock waves originating from, for example, a cowl and a strut, glance across boundary layers growing along inner walls of the intake (see, for example, Ref.2). Once the interaction is strong enough to cause separation, several undesirable phenomena may impair the intake performance and could result in a critical deterioration of the air-breathing engine thrust. The interactions formed at wing/fuselage junctions and at hinge lines of the deflected control surfaces also lead to several problems such as reduction of the effectiveness of the control surface and deterioration of the lift-drag efficiency. Local heating can cause problems in both outer and inner flows especially at hypersonic speeds.

In order to understand the interaction behaviour, a number of experimental, theoretical and numerical studies have been undertaken by means of several models with relatively simple geometries. The interaction induced by a sharp fin (see Fig.1-2) is one of the most popular glancing interactions and it is the configuration with which this study begins. The broad features of sharp-fin-induced interaction have been described by many authors including Stanbrook⁽³⁾, McCabe⁽⁴⁾, Korkegi⁽⁵⁻⁷⁾, Oskam et al.⁽⁸⁾, Token⁽⁹⁾, Zheltovodov⁽¹⁰⁾, Zubin and Ostapenko⁽¹¹⁾ and Kubota⁽¹²⁾. More recently, particular features have been studied such as conical similarity (Settles and Lu⁽¹³⁾, Lu and Settles⁽¹⁴⁾ and Settles and Kimmel⁽¹⁵⁾), upstream influence (Dolling and Bogdonoff^(16,17), Dolling⁽¹⁸⁾ and Lu and Settles⁽¹⁹⁾), inception length (Lu and Settles⁽²⁰⁾), flow unsteadiness (Tan et al.⁽²¹⁾, Schmisser and Dolling⁽²²⁾ and Garg and Settles⁽²³⁾) and heat transfer (Lee and Settles⁽²⁴⁾ and Haq⁽²⁵⁾). Detailed three-dimensional flowfield

surveys using optical methods have been made by Lu and Settles⁽²⁶⁾, Hsu and Settles⁽²⁷⁾, Alvi and Settles^(28,29) and Smith et al.⁽³⁰⁾, and there have been parallel developments of CFD codes by Horstman and Hung⁽³¹⁾, Horstman⁽³²⁾, Knight et al.⁽³³⁻³⁵⁾, Hung⁽³⁶⁾ and Panaras⁽³⁷⁾. Useful summaries of much of this work have been made by Stollery⁽³⁸⁾ and Settles and Dolling^(39,40). As a starting point for the present study, the following sections will survey some important discoveries about sharp-fin induced glancing interaction made by the investigators mentioned above.

1.1.1 Boundary layer separation

Fig.1-3 shows sharp-fin induced surface streak patterns obtained at different interaction strengths^(12,13). When the interaction is weak, surface streaks do not form a clear oil convergence (see Fig.1-3(a)). But once a fin-generated shock wave reaches a certain strength, a separation of the boundary layer known as "primary separation" is caused by the pressure gradient imposed by the shock wave. Primary separation originates from the line of oil convergence and terminates at the line of divergence, which are treated as a separation and a reattachment line, respectively (see Fig.1-3(b)).

In the early stages, many investigators spent a considerable time trying to establish the criterion for the "incipient primary separation (IPS)". MacCabe⁽⁴⁾ assumed that the IPS occurs when the deflected surface-flow becomes aligned with the shock wave. By equating the theoretical surface-flow deflection angle to the oblique shock angle, the fin half-apex angle (or shock-generator's angle of attack) to induce the IPS, α_{IPS} , can be calculated as a function of the free-stream Mach number. Korkegi⁽⁵⁾ approximated this criterion by a very simple relation;

$$M_{\infty} \alpha_{IPS} = 0.364 \quad (1-1)$$

where α_{IPS} is expressed in radians. Furthermore, Korkegi found that

$$M_{\infty} \alpha_{IPS} = 0.3 \quad (1-2)$$

gave better agreement with several experimental data^(5,6). Fig.1-4 shows Korkegi's

criterion expressed by Eq.(1-2) with several fin angles which caused the IPS reported by many investigators. There is a reasonable agreement between the experimental data and eq.(1-2).

Later, a number of researchers (for example, Kubota⁽¹²⁾) pointed out that the interaction region flow field can still be attached even when the surface-flow deflection angle exceeds the swept shock angle (see Fig.1-3(a), in which the shock angle is marked by "cal. shock"). Kubota judged the formation of the IPS by means of the appearance of the "complete" surface-streakline convergence (see the part of the oil convergence labelled "c.c." in Fig.1-3(b)). Contrary to his observation, a weak separation was believed to be created by a lower angle than that from eq.(1-2) (see, for example, Refs.25,41 and 42). But the oil coalescence created by this weak separation does not necessarily correspond to the "complete" convergence line that Kubota defined. These uncertainties are due partly to the fact that the IPS is quite difficult to distinguish from surface streak patterns and partly to an uncertainty in the definition of the IPS itself.

In addition, Korkegi's criterion ignores the Reynolds number dependence related to the characteristics of the incoming boundary layer. Degrez and Ginoux⁽⁴³⁾ proposed a simple relation for the fin angle needed to induce the IPS with laminar boundary layers;

$$\alpha_{IPS} = \frac{37\sqrt{M_\infty}}{Re_{XF}^{0.22}} \quad (1-3)$$

where Re_{XF} is based on distance from plate leading edge (on which the fin is placed) to the fin apex. Another proposal by Zheltovodov et al.^(40,44) is applicable to both turbulent and laminar boundary layers;

$$\frac{P_p}{P_\infty} = \frac{K_1 M_{nse}^2 C_f^{\frac{1}{2}}}{(M_{nse}^2 - 1)^{\frac{1}{4}}} + 1 \quad (1-4)$$

where P_p is the "plateau" pressure, $K_1=5.94$ for turbulent and 1.46 for laminar flows, and M_{nse} is the Mach number normal to the separation line. The effect of

Re is included in the skin-friction coefficient C_f , which has a weaker Re dependence for turbulent flows than for laminar flows. Based on eq.(1-4), they proposed the level of pressure rise needed to cause the IPS. According to the studies of Degrez and Zheltovodov, the pressure level for the IPS tends to decrease with Re . But this Re effect is still ambiguous and further investigation is required.

Despite these uncertainties, eq.(1-2) is a broadly-accepted practical criterion for the starting of a significant vortical separation, which has importance for engineering design. Hence Korkegi's criterion, eq.(1-2), will be used in Chapter 5 in order to predict the necessary geometrical requirements needed to prevent significant vortical separation.

The oil flow feature defining "secondary separation" appears when the interaction has become more strong (see Fig.1-3(c)). Zheltovodov et al.⁽¹⁰⁾ were one of the first observers of this behaviour and have invested considerable effort in trying to understand it. According to their observations, the criterion for incipient secondary separation (ISS) can also be approximated by

$$M_\infty \alpha_{ISS} = 0.6 \quad . \quad (1-5)$$

In Fig.1-4, eq.(1-5) is expressed as a double-chain-dotted line with some experimental data from Ref.10 marked by solid circles.

Secondary separation does often appear without accompanying of secondary attachment (Fig.1-3(c) corresponds to this situation). Settles and Dolling⁽⁴⁰⁾ suggested that this topological-impossible situation may be due to a negligible location between secondary separation and attachment, such that they appear as a single feature. This suggestion is supported by the fact that Zeltovodov observed both secondary separation and attachment clearly in an extremely-strong interaction experiment at $M_\infty=4$, $\alpha=30^\circ$ (This case is reported in Ref.40 but the corresponding surface streak picture has not been published). On the other hand, Hung proposed (based on his numerical study⁽³⁶⁾) that a drastic change of skin friction in both magnitude and direction could cause a temporary accumulation

of oil which would resemble a line of secondary separation. Whether the "secondary separation" line indicates an actual separation or something else must be further investigated.

1.1.2 Flowfield structure

Beside the investigations of incipient separation, attempts have been made in parallel to explore the whole interaction flowfield. In order to understand the flowfield structure, three- dimensional surveys are necessary. Several researchers (for example, Oskam⁽⁸⁾, Shapey and Bogdonoff⁽⁴⁵⁾) have attempted to define the interaction flowfield structure by way of, for example, pitot- and yaw-probe surveys and have identified some important features such as the bifurcation of the inviscid shock into a λ foot and the development of a separation vortex (see Fig.1-5). However, the resolution of the data from intrusive probes was not fine enough to understand the detailed flow features. The probes could also disturb the flowfield itself to a certain extent.

The optical flowfield visualizations are very attractive tools to survey the interaction flowfield structure. Zubin and Ostapenko⁽¹¹⁾ did the first conical shadowgraph experiments for sharp-fin interactions and made clear the main details of the shock structure, wall pressure patterns and the surface streak features. More recently, extensive optical studies have been carried out using conical holographic interferometry⁽²⁷⁾, conical shadowgraphy⁽²⁸⁾ and laser-light-sheet visualization^(26,29). These studies have provided a very clear view of the detailed flowfield structure. The following section summarises the results.

Fig.1-6 exhibits some views of the flow field obtained using three different optical methods. Fig.1-7 is a schematic flow structure derived from the optical observations. The separation shock, originating around the position of the primary separation line labelled S_1 in Fig.1-7, impinges upon the inviscid shock wave and leads to the λ -shaped shock structure. The boundary layer separates from the flat plate at S_1 and rolls up to form a spiral vortex. If the interaction is strong enough to cause secondary separation, a small vortex rotating in the opposite sense may

be formed around S_2 as shown in Fig.1-7.

The stream tube bounded above by a slipline originating from the triple point and below by the primary separation vortex was discovered by Lu and Settles⁽²⁶⁾. After passing through the rear shock, it turns downward along the separation vortex by means of a series of Prandtl-Meyer expansion and compression fans, and impinges directly upon the flat plate at the position of the reattachment line A_1 . This impinging causes peak heating, pressure and skin friction around A_1 as observed by many investigators. As long as the interaction is weak, this turning process is essentially isentropic. But as the interaction strength increases, this process can no longer remain isentropic. The compression finally becomes a **normal shock wave**.

After impingement at A_1 , the stream tube divides into two streams, one turns forward into the primary separation vortex (leftward direction in Fig.1-7) and the other turns towards the fin. In the "reversed" flow direction (the leftward direction), the strong pressure gradient existing around A_1 accelerates the flow. For very high interaction strengths, this reverse flow reaches supersonic speeds locally (**supersonic reversed flow**) with an accompanying small λ shock structure around S_2 .

Alvi and Settles⁽²⁸⁾ classified the interaction flow regimes by Mach number component normal to the inviscid shock (M_n) and static pressure ratio before and behind the shock. Fig.1-8 presents their classification with sketches explaining each regime. In this figure, the conditions for the boundaries of primary and secondary separations are from eqs.(1-2) and (1-5), respectively. "Normal shock" and "supersonic reversed flow" correspond to the flow conditions identified by the bold words mentioned above. The biggest advantage of this chart is that one can take into account the effects of both α and M_∞ on the interaction at a same time by means of a single index, M_n .

The present experiment conducted at $M_\infty=2.46$ using an unmodified sharp fin of $\alpha=15^\circ$ corresponds to an early stage of the secondary separation regime (i.e. incipient secondary separation, see the solid circle in Fig.1-8). The actual

behaviour of this interaction will be discussed in Chapter 3.

1.2 Several control methods and the effects of junction modifications on the interaction

In order to control the shock-boundary layer interaction, several methods have been considered. These methods can be classified into two categories;

1) Methods acting on the boundary layer.

Wall cooling, passive control, air suction and injection belong to this category. These techniques can be applied upstream and in the interaction zone to make the boundary layer more resistant to the adverse pressure gradient caused by the shock wave system.

2) Methods acting on the inviscid shock wave.

Geometry modifications can weaken the "inviscid" shock wave directly and so weaken the shock wave/boundary layer interaction.

Delery⁽⁴⁶⁾ summarized several control methods belonging to category (1) but most of them are restricted to two-dimensional cases and three-dimensional applications are very limited. The passive control technique has been widely investigated mainly for weakening shock/boundary layer interaction on transonic airfoils⁽⁴⁷⁻⁵⁰⁾(see Fig.1-9). In this technique, part of the airfoil upper surface is replaced by a porous wall below which a cavity is installed and the interaction is weakened by a natural circulation from the downstream side (high pressure) to the upstream side (low pressure) of the interaction. Saida⁽⁵¹⁾ applied this method in order to weaken the interaction induced by a forward-facing step placed on the wall of a supersonic wind tunnel. So far studies of the passive control technique have been limited to two-dimensional applications.

Air suction and injection also belong to the first category. When a sharp fin placed in a duct in which the flow is supersonic (corresponding to, for example,

a supersonic air intake) is considered, two types of interaction between wall boundary layers and a plane oblique shock originating from the fin are expected in the regions marked A and B in Fig.1-10 . Air suction is quite useful in preventing separation induced by the oblique shock impinging on the wall in the region A (two-dimensional case). Many studies have been carried out to investigate the suction effect for the interaction at A by means of, for example, a porous wall⁽⁵²⁾ and slot⁽⁵³⁾. Using these methods, the interaction-induced separation can be eliminated. A useful summary of much of this work has been made by Hamed and Shang⁽⁵⁴⁾.

Contrary to the effectiveness for the interaction at the region A, it seems to be much more difficult to control the three-dimensional glancing interaction developed in the region B and the number of studies is limited. Peake and Rainbird⁽⁵⁵⁾ investigated the effects of tangential injection on the interaction experimentally with $\alpha=8^\circ$ and 11.5° at $M_\infty=2.0$ (see Fig.1-11(a)) but they needed an extremely high-speed injection parallel to the wall in order to delay separation. Gaintonde and Knight⁽⁵⁶⁾ studied the effect of air-suction through a porous plate in the interaction region numerically at $M_\infty=2.95$, using a sharp fin geometries with $\alpha=8^\circ$ and 20° . They concluded that the effect of suction was limited to the vicinity of the bleed area and did not alter the overall flowfield in any significant fashion. On the other hand, Shizawa et al.⁽⁵⁷⁾ showed the beneficial effect of suction through a porous plate experimentally (see Fig.1-11(b)). In some of their cases, the λ -shaped shock structure was almost suppressed by suction and the static pressure distribution approached the inviscid value (see Fig.1-11(c)). This case was carried out at $\alpha=15^\circ$ and $M_\infty=3.25$ with bleed ratios, expressed as a fraction of boundary layer mass flow, of 7.7% and 10.1%).

Geometric modifications of the shock-wave generators belong to category (2). They can weaken the inviscid shock wave which leads to a reduction of the size and strength of the interaction. Hussain⁽⁵⁸⁾ and Fomison and Stollery⁽⁴²⁾ investigated the effect of sweep on sharp and blunt-fin-induced interactions. Whilst Koide⁽⁵⁹⁾, Rodi and Dolling⁽⁶⁰⁾ and Haq⁽²⁵⁾ studied the effects of fin-leading-

edge geometry.

Fillets and strakes are used extensively to modify the air flow over aircraft at subsonic speeds, particularly in the neighbourhood of wing-body junctions. Although they might also be considered attractive for decreasing the interaction strength at supersonic and hypersonic speeds, available data are limited. Figs.1-12~14 shows some test models introduced to investigate the effects of junction modifications in the highspeed regime. Test conditions employed for these investigations are listed in Table 1-1. Lakshmanan and Tiwari⁽⁶¹⁾ investigated the effect of a circular fillet on the flowfield around a blunt fin/flat plate junction numerically (see Fig.1-12). They found that a fillet with a radius of 3.5 times the fin leading edge diameter was required in order to weaken the horseshoe vortex formed in the interaction region. Haq⁽²⁵⁾ examined the heat transfer behaviour using the same model as Lakshmanan but at a higher Mach number. His fillet alleviated the heating distribution around the fin and reduced the extent of disturbed flow. On the other hand, Blank⁽⁶²⁾ investigated a triangular-ramp-type fillet for an unswept and a swept blunt fin experimentally (see Fig.1-13(a)) and concluded that the fillet enlarged the size of the disturbed flow region rather than diminishing it. Whilst his strake-type modifications for the unswept blunt fin (see also Fig.1-13(a)) could reduce the pressure levels and shock fluctuations around the strakes to a certain extent but the reduction was limited to the vicinity of the strake⁽⁶³⁾. Once the strong bow shock caused by the unmodified part of the fin reached the wall, strong separation occurred with a vivid oil convergence similar to that for the unmodified fin (see Fig.1-13(b), reprinted from Ref.63). Kleifges⁽⁶⁴⁾ tested two different strakes shown in Fig.1-14 for an unswept fin with emphasis on the flow unsteadiness. The strake whose width was same as the fin (see Fig.1-14(a)) brought a considerable reduction in the size of the interaction and its unsteadiness ahead of the strake but the effect was also limited in spanwise extent. The effect of thinner strake (see also Fig.1-14(b)) was too small to be detected.

At low subsonic speeds, several data are available for junction modification

effects on the flow fields around wing-body junctions (see Figs.1-15,16 and Table 1-2, Kubendran and Harvey⁽⁶⁵⁾, Sung et al.⁽⁶⁶⁾ and Devenport et al.^(67,68)). These investigations showed poor effectiveness of a fillet in decreasing the strength of the junction horseshoe vortex. In contrast a strake was shown to be quite effective in reducing the strength of the vortex. However no data are currently available for the effect of a fillet and a strake on sharp-fin-induced interaction at either subsonic or supersonic speeds except Ref.69, which includes the core part of the present experimental observations and a preliminary study of the analysis discussed in Chapter 5 of this paper.

1.3 The Present study

The effects of junction modifications on the glancing shock wave/turbulent boundary layer interaction generated by a sharp fin placed on the wall of a supersonic wind tunnel were examined experimentally at a Mach number of 2.46 and a Reynolds number of $2.59 \times 10^6/m$.

The aims of this study were

- 1) to find a junction modification technique to prevent or weaken separation caused by a sharp-fin-induced glancing shock wave/turbulent boundary layer interaction.
- 2) to understand the modified-flowfield structures.
- 3) to predict the necessary geometric requirements to prevent separation.

In order to find an effective modification technique, the interactions between a turbulent boundary layer and shock-wave systems caused by a fin with a fillet and several fins with different strakes were examined individually. The flow features obtained by oil flow visualization, surface pressure measurements and liquid crystal thermography were compared with the data from an unmodified fin to evaluate the effects of each modification. To understand the flowfield structures,

three-dimensional surveys using laser-light-sheet flow visualization (LFV) were employed with schlieren photography and oil flow visualization. In addition to these experimental observations, an Euler CFD solver was introduced to help understand the inviscid flow structures which play important roles in the interaction behaviour. Based on the data obtained experimentally and numerically, a method has been proposed for predicting the junction shapes needed to prevent separation.

CHAPTER 2 Experimental arrangement

2.1 Test matrix

Five types of experiments were conducted using three groups of test models and two supersonic wind tunnels. Table 2-1 shows the complete test matrix for the present research programme. The core experiments were carried out using the group A models in the 9"×9" tunnel. The following section describes the wind tunnels, test models and the experimental techniques (except for the rhombic delta wing tests and the CFD calculations which will be discussed later in the appropriate sections).

2.2 Wind tunnel facilities and the test conditions

2.2.1 Wind tunnels

(1) 9"×9" tunnel

Most of the experiments were conducted in a continuous supersonic wind tunnel with a test section area of 228.6mm×228.6mm (9"×9") located at the end of a two dimensional asymmetric supersonic nozzle (Fig.2-1). Test Mach numbers can be varied from 1.7 to 2.8 by means of changing nozzles. The tunnel is powered by two 373 kw electric motors driving a centrifugal compressor.

The stagnation pressure can be automatically maintained between 1.38×10^4 Pa and the ambient pressure (2.0 and 14.7psi). By regulating the flow of water in the tunnel cooling system, the stagnation temperature can be controlled in the range from 5K below to 10K above the ambient temperature.

(2) 2.5"×2.5" tunnel

Liquid crystal thermography tests (see Sec.3.3 in Chapter 3) and Rhombic delta wing tests (see Sec.5.2 in Chapter 5) were conducted in an intermittent tunnel with a cross section area of 63.5mm×63.5mm (2.5"×2.5") (see Fig.2-2). By

changing asymmetric supersonic nozzles, a range of test Mach numbers from 1.6 to 3.5 is available.

The wind tunnel is an indraft tunnel driven by air at atmospheric pressure sucked down into an evacuated tank. The low pressure tank has a volume of approximately 45m^3 ($1,600\text{ft}^3$) and is evacuated by a 25 kilowatt electrically driven pump.

2.2.2 Test section flow conditions

(1) 9"×9" tunnel

The tunnel stagnation pressure was nominally maintained at 4 psi throughout the test program by means of the pressure gauge mounted on the central control panel. In order to obtain accurate measurements of stagnation pressure, a pressure transducer, Druck PDCR10 (range ; $1.03 \times 10^5 \text{Pa}$ [15psi] abs), was connected to the settling chamber. The stagnation pressure remained almost constant during the test runs and was $2.48 \times 10^4 \text{Pa} \pm 1.3\%$.

The test section static pressure was read from another Druck PDCR10 transducer, which was connected to a pressure tap drilled slightly upstream of the model mounting position. For the present test, the nozzle for a nominal Mach number 2.5 was employed. The actual Mach number, calculated from the stagnation and static pressures and using the isentropic relation, was $2.46 \pm 1.1\%$.

The stagnation temperature read by a Chromel-Alumel thermocouple installed in the tunnel settling chamber was $290\text{K} \pm 1.3\%$.

The freestream unit Reynolds number was calculated as $2.59 \times 10^6 / \text{m} \pm 4.4\%$. The measured stagnation temperature and calculated Mach number provided the freestream static temperature. Sutherlands' law of viscosity was used to calculate the coefficient of viscosity. Density was obtained from the equation of state and the velocity was derived from Mach number and the speed of sound.

The error for each flow property was estimated based on the accuracies of the measuring equipments and the repeatabilities of the test conditions throughout the test programme.

A fully developed turbulent boundary layer growing along the tunnel wall was employed as the test boundary layer. The properties of the boundary layer were thoroughly investigated by Kubota⁽¹²⁾. He carried out pitot and static pressure surveys at three locations across the test region marked A, B and C in Fig.2-3. The static pressure across the undisturbed boundary layer was constant and equal to the value at the tunnel wall. The boundary layer properties and velocity profiles are reproduced in Figs.2-3 and 2-4 from Ref.12. The boundary layer thickness δ defined by a point where 99.5% of the external flow velocity was reached was 15.7mm (0.62") and the corresponding displacement and momentum thicknesses, δ^* and θ , were 3.8mm (0.15") and 1.0mm (0.04") respectively. The velocity profile was close to a 1/7 power law.

(2) 2.5"×2.5" tunnel

The stagnation pressure read by a pressure transducer (Druck PDCR 10/F-1, range ; 1.03×10^5 Pa [15psi] abs) installed in the tunnel settling chamber was 9.76×10^4 Pa $\pm 0.9\%$, which was approximately 3kPa (0.44psi) lower than atmospheric due to the pressure drop thorough the silica-gel drying system. The static pressure was read by another Druck PDCR 10/F-1 transducer connected to a static tapping placed on the test-section top liner of the tunnel. The Mach number calculated by the isentropic relation was $2.45 \pm 1.3\%$. The stagnation temperature obtained by a Chromel-Alumel thermocouple located in the tunnel settling chamber was $290\text{K} \pm 0.8\%$. The Reynolds number was calculated to be $1.02 \times 10^7/\text{m} \pm 3.7\%$ by means of the same way applied to the 9"×9" tunnel testings.

The boundary layer growing along the tunnel side wall was a fully developed turbulent boundary layer. The values of δ , δ^* and θ obtained by Kubota⁽¹²⁾ in the same manner to the 9"×9" tunnel were 5.1mm (0.2"), 1.8mm (0.07") and 0.38mm (0.015") respectively.

2.3 Test models

2.3.1 Group A

Fig.2-5 shows the five models employed in order to evaluate the effects of several junction modifications. Each model was designed asymmetrically in order to obtain the largest model size without choking the tunnel (about 10% of the tunnel test section). The flowfield of the test side (the lower side of each model in Fig.2-5) was not affected by the other side. The shaded area of each model shows the modified portion. For convenience, the unmodified part of the model (equal to the unmodified fin) is termed "fin" and all variables related to this part are expressed using a suffix "fin" in the following discussions. On the other hand, all variables related to the strake are identified by a suffix "str".

To provide "standard" data for sharp-fin induced interaction, a fin with a 15° half-apex angle (α_{fin}) was employed as the unmodified model (Fig.2-5(a)). The angle was chosen to create a fully-separated interaction flowfield based on the observations from previous investigations, such as those of Korkegi⁽⁷⁾ and Kubota⁽¹²⁾. Based on eqs.(1-2) and (1-5), this apex angle is thought to create not only primary separation but incipient secondary separation as well (see the solid circle in Fig.1-8). The half-apex angle of the fillet, 25° , was selected as a mean angle between α_{fin} and the inviscid shock wave angle calculated by the oblique shock relations based on M_∞ and α_{fin} . The cross sections of the fillet are circular on planes perpendicular to the model axis (see Fig.2-5(b)). The dimensions of the strakes are shown in Figs.2-5(c),(d) and (e). Each strake length (L_{str}) was selected based on the size of δ , i.e. 2.0δ for Strakes 1 and 2 and 3.0δ for Strake 3. The strake height (H_{str}) of 66mm for Strakes 2 and 3 was determined to make the angle ρ (see Figs.2-5(d),(e)) more than the Mach angle, μ_∞ . This angle was expected to prevent the shock wave caused by the unmodified part of the model leading edge (fin shock) from affecting the flow on the wall until downstream of the trailing edge.

2.3.2 Group B

Three more fin-strake combinations were studied using oil flow visualization and the CFD procedure. The results obtained from these investigations were used in Chapters 4 and 5 in order to progress the physical understanding of the flowfield behaviour and to predict the strake geometries required to avoid separation. The methods of specifying the configurations of Group B rely on the predictions discussed in Chapter 5 and are explained in that chapter. Hence this section briefly explains the dimensions of each model.

Strakes B1 and B2 have a longer L_{str} with the same fin length (L_{fin}) and α_{fin} as those of Strakes 2 and 3. Top views of the two models are quite identical, so that only one top view is presented for them (see Figs.2-6(a),(b)). In order to introduce larger ratios of L_{str}/L_{fin} and H_{str}/L_{fin} whilst maintaining the blockage ratio of around 10%, Strake B3 has a 25.4mm (1") shorter L_{fin} with the same α_{fin} (see Fig.2-6(c)). All models of Group B have taller heights than those in Group A in order to maintain a certain length for the unmodified part of the model leading edge.

2.3.3 Group C

For liquid crystal testing, three small models were additionally manufactured for the 2.5"×2.5" tunnel. Their cross sections were maintained less than 9% of the tunnel test section area in order to avoid the tunnel choking. Their shapes are similar to the unmodified fin, Strake 2 and Strake B2 (see Fig.2-7) and are named Unmodified C, Strake 2C and Strake B2C, respectively.

2.4 Experimental techniques

2.4.1 Surface oil flow visualization

To visualize the streak patterns on the tunnel side wall and the model surface, the oil flow technique using a mixture of titanium dioxide and motor oil was employed. Several drops of oleic acid were also added to avoid coagulation

of the titanium dioxide. The disk and the model were painted matt black to provide the clearest photographic contrast and were mounted on the tunnel side wall (see Fig.2-1(a)).

Pictures of the side wall patterns were taken whilst the tunnel was running. Oil patterns on the model surface were recorded after tunnel shut down. The disk plus model was removed after each run and close up pictures of the oil flow patterns were taken. Tunnel shut down did result in some oil spatter and slight smearing of the surface pattern, but the change of the key flow features was very small. To obtain quantitative information about key features (e.g. separation line, reattachment line et al.), a transparent film was placed on the disk and model surfaces individually, and oil flow patterns were carefully copied on the film using a felt-tip pen. By this technique, full-sized, undistorted traces could be obtained.

2.4.2 Surface pressure measurement

Detailed surveys of the surface pressure distributions in the interaction region were achieved by means of 225 pressure tappings arranged in 19 rows on a disk mounted on the side wall (see Fig.2-8). The intervals of the tapping are 12.7mm (0.5") in the flow direction and 6.35mm (0.25") in the lateral direction. The diameter of the tappings is 0.5mm.

The pressure tappings were connected to a Scanivalve pressure scanning system with a Druck PDCR 22 pressure transducer (range; 1.03×10^5 Pa [15psi] abs). The static pressure taken from a tapping in the side wall just upstream of the disk was read as an undisturbed upstream value. Outputs from the pressure transducer were amplified, digitized and stored on a floppy disk by a DIGITAL MINC/DECLAB23 system. In order to double the "density" of pressure tappings, another set of data was collected with the model placed 6.35mm (0.25") forward of the original position (see the position marked by a broken line in Fig.2-8), and was combined with data obtained at the original position.

2.4.3 Liquid crystal thermography

Liquid crystals (LCs) can change their colour according to the surrounding temperature. The change is reversible and repeatable as long as LCs are not damaged physically. This very attractive feature is achieved by LCs' helical molecular structure whose pitch length varies with the temperature variation. When LCs are illuminated with white light, they reflect light of a particular wavelength which changes in response to the pitch length. The surface beneath the LC layer must be painted matt black or must be manufactured from a black material to absorb transmitted light. In order to employ LCs for highspeed aerodynamic testing with high shear stresses, LCs must be microcapsulated by means of polymer shell. The principles of LCs and their test techniques are detailed in Refs.25 and 70.

Several investigations have been carried out using LCs for heat transfer analyses related to shock wave/boundary layer interaction^(25,71-73), but all of them have been conducted at hypersonic speeds. Short-duration hypersonic facilities, whose test times were less than one second, were used for most of the testings. Under such circumstances, LCs tend to have a clear colour change because the difference between the wall temperature and the flow recovery temperature is very large and the interaction itself is also quite strong. On the other hand, the author does not know of any successful results obtained at supersonic conditions from intermittent or continuous tunnels. In several trials in the Cranfield^(12,58), no sharp image has been obtained.

In order to increase the difference between the wall temperature and the flow recovery temperature artificially, a thin foil-heater was introduced for the wall of the 2.5"×2.5" tunnel in the present test programme. After a few trials, Minco's thermofoil heater, HK5170R123L12D, was finally selected. This 50.8mm×76.2mm×0.25mm heater is a thin, flexible, heating element consisting of an etched-foil resistive element laminated between layers of flexible electric insulator (see Fig.2-9). It was thought suitable for the present purpose because of its large heat flux capability with a large resistance (123Ω at room temp.) and a

small thermal mass of the heater for quick heating. Furthermore an 0.076mm aluminium foil was attached to the surface in order to enhance uniform heating (see the silver side of the heater in Fig.2-9).

The foil heater was bonded on the insulator board made of syntactic epoxy, ADVANCED COMPONENT Grp. ACG TB 650, whose thermal conductivity is 0.099W/m/K. The bonding was carried out by means of 0.025mm acrylic film under a high temperature-pressure environment (about 180°C, 1.4×10^6 Pa). Between the foil and the insulator, two quick response thermocouples (RS Type K) were embedded in order to provide the reference temperatures. Fig.2-10 shows the heater assembly finally used.

The assembly was then installed into the tunnel wall (see Fig.2-11). At first, no insulation was placed around the assembly, but an unacceptable heat loss toward the wall destroyed the temperature uniformity over the heater. Hence further insulation was placed around it. The model was held by a sting in order to prevent physical damage to the heater. The heat loss to the model was also large, so that all models of Group C were remade from an insulation material. These models and the insulator around the heater assembly were made of the syntactic epoxy, ACG TB 650.

The LCs used here were TCS811 manufactured by MERCK-BDH with a colour-changing temp. range of 27.6°C~41.5°C and a red start temperature of 28.4°C. Exact colour changing temperature bands are listed in Table 2-2. About 0.5ml of LCs were sprayed on the heater carefully using an air brush. The heater temperature was maintained around 35°C (corresponding to light blue) before the tunnel was started. The supply voltage was varied during the run in order to find the most vivid LC colour difference. Colour changing was recorded using a Panasonic NV-S7B super VHS video system operating at 25 frames per second.

The foil heater was powered thorough leadwires connected to a DC variable transformer which was capable of an output voltage up to 240V. The voltage applied to the heater was recorded during the testing and the reference temperatures from the thermocouples embedded under the foil were also noted.

2.4.4 Schlieren photography

Shock wave patterns were observed two-dimensionally by the conventional single-path schlieren photographic technique. The light source was a PHOTONIC ANALYSIS PALFLASH 500, which provided a single flash of 100 nanoseconds to one microsecond duration. All models were mounted on the optical window horizontally by a tunnel sting in order to obtain a light path. The gap between each model and the window was quite small. In addition vacuum grease was stuffed into the gap, so that the flow leakage through this gap was considered to be negligible.

2.4.5 Laser light sheet flow visualization (LFV)

Three-dimensional flowfield surveys were made using the LFV technique (see Fig.2-12). In the surveys, a very thin laser-light-sheet was introduced perpendicular to the unmodified inviscid-shock direction through the tunnel side window and illuminated the flowfield containing the visualization seeding. The location of the sheet could be moved parallel to the unmodified inviscid-shock direction by traversing a table on which the optics were mounted. To obtain the camera view perpendicular to the light sheet, each model was mounted on the tunnel roof (see Fig.2-13). The model and the tunnel roof were painted matt black in order to reduce the reflection of the laser light from the solid surfaces. It should be mentioned that several white markers on the model surface shown in Fig.2-13 were temporary lines in order to arrange the system. They were removed before the LFV images were taken. From a series of LFV images obtained at several different locations, a three-dimensional flow structure could be deduced.

As a light source, a 4W Argon-Ion laser (COHERENT INNOVA 70-4, see Fig.2-14) was used. To create a thin laser-light-sheet, an optical system consisting of a pair of spherical lenses and a pair of cylindrical lenses was employed (marked S1, S2, C1 and C2 respectively in Fig.2-15). The optical arrangement is shown in Appendix A. With the optical setup finally obtained, the light sheet had a minimum thickness of less than 1 mm at the focal point. The sheet thickened

slightly on either side of the focal point, but this divergence was gradual and the light sheet thickness remained limited to 1.5 mm within the whole test section. By changing the distances between the lenses, the height of the sheet and the focal-point location could easily be altered (see the details in Appendix A). The height was chosen slightly taller than the model height and the focal point was placed near the unmodified inviscid shock location estimated by oblique shock theory.

Flow seeding is needed for the LFV technique. Water vapour, produced by a commercially available boiler, was injected into the tunnel settling chamber through the vapour injectors installed in the top and bottom of the chamber (see Fig.2-16(a)) whilst the tunnel was running. Because the total pressure inside the tunnel was lower than ambient pressure, water vapour was sucked into the tunnel without any boosting. When the water vapour travelled through the supersonic nozzle, it turned into numerous small ice particles which were expected to work as the visualization seeding. At an early stage of this testing, water vapour was injected solely through injector A (see Fig.2-16(b)). However the dispersion of the vapour was insufficient to give a uniform distribution across the test section. Hence another injector, Injector B (see Fig.2-16(c)), was added at the top of the settling chamber. The vapour injection system finally arranged provided a reasonably even distribution of seeding particles.

A Kodak Tri-X Pan film (ASA400) was used to photograph the flowfield images. Pictures were taken at shutter speeds of $1/4 \sim 1/30$ sec and apertures of $f/4 \sim f/5.6$, which must be adjusted according to the images.

McGregor⁽⁷⁴⁾ and Snow and Morris⁽⁷⁵⁾ investigated advantages and disadvantages of several visualization seedings, light sources and test arrangements for vapour screen methods to which the LFV belongs. More recently, the details of the LFV technique used for the sharp-fin-induced interaction experiments of Alvi and Settles⁽²⁹⁾ was presented. It was from their work that the present light-sheet generator design was derived. However suitable test arrangements are strongly dependent on the tunnel features and test

conditions, so that their techniques did not necessarily suit the present test programme directly. Numerous trials were required to establish techniques suitable for our own experiments.

2.5 Coordinate systems

In order to present the experimental and computational results, two different types of coordinate systems were employed (see Fig.2-17). In both systems, the origins coincide with the fin leading edge on the tunnel wall. The X , Y and Z axes are defined in directions parallel and normal to the model axis. X_s and Y_s are along and perpendicular to the unmodified inviscid shock wave direction calculated by the oblique shock relations. In addition, a length X_f is also employed to specify a distance from the fin leading edge along the fin-wall junction as shown in Fig.2-17.

CHAPTER 3 Surface flow features and evaluating the effects of the modifications

In order to evaluate the effects of several junction modifications, oil flow visualizations, surface pressure measurements and liquid crystal thermography testings were undertaken. The experimental results are presented in the following sections and the effectiveness of each modification will be discussed.

3.1 Oil flow patterns

Oil flow patterns on the wall for an unmodified model and four modified models are presented in Fig.3-1. Fig.3-2 compares the positions of the primary separation lines for the five cases shown in Fig.3-1.

The surface oil flow pattern for the unmodified fin has primary separation and reattachment lines. Between the two lines, another oil accumulation labelled "secondary sep. & reatt." is detected. Looking at this accumulation carefully, it has been observed that oil streaks actually converge into and diverge from the accumulation (see the sketch attached to Fig.3-1(a)). This may suggest that the secondary separation and reattachment lines locate very closely each other, so that these two lines seem to be a single line as suggested in Ref.40. Hence in this thesis, this accumulation is treated as separation and reattachment for secondary separation. However the convergence and divergence along this accumulation are so weak that secondary separation is considered to be its incipient condition. In Kubota's experiments⁽¹²⁾, the same kind of accumulation occurred between α_{fin} of 13° and 15° . Based on these observation and the solid circles in Fig.1-8, Zheltovodov's criterion for secondary separation expressed by eq.(1-5) is considered to be reasonable.

The model with a fillet has a similar oil flow pattern to that of the unmodified case (see Fig.3-1(b)), but the position of the primary separation line is shifted slightly upstream (see Fig.3-2). This shifting may indicate the

enlargement of the separation vortex and the fillet is considered to strengthen the interaction rather than weaken it.

In the case of Strake 1, the flow features are almost the same as for the unmodified case except for the flow pattern in the vicinity of the strake. In addition there is a bold oil streak just downstream of the primary separation (indicated by "joint streak" in Fig.3-1(c)), which originates slightly upstream of the junction of the strake and the fin (labelled "strake joint" in the same figure). Looking at the oil pattern carefully, the joint streak is not a convergence line except at the vicinity of the strake joint. The cause and features of this streak will be discussed at Sec.3.4 but the flow pattern suggests that the joint streak is attributed to the oil accumulation formed by a compression at the strake joint.

The flow features for the model with Strake 3 are different from the first three cases (see Fig.3-1(e)). At a certain distance from the model, the primary separation line changes from what Kubota termed a "complete" line into an "incomplete" line, where the oil streak lines never fully converge into a single line⁽¹²⁾ (see Fig.1-3(b), i.c. and c.c. in the figure correspond to the incomplete and complete convergence line respectively). In Kubota's case, primary separation was very weak when incomplete and complete lines existed together. Hence primary separation is thought to be weakened by this strake and there is no sign of secondary separation. Strake 2 has a similar oil flow pattern (see Fig.3-1(d)) and the oil convergence into the primary separation line is not strong compared with the first-three cases, but the line is still complete.

Besides this change in "completeness", Strakes 2 and 3 have primary separation lines which are closer to the model than for the first-three cases (see Fig.3-2). When the strake is introduced, the interaction region may be dominated by a weaker shock from the strake leading edge (strake shock). Hence the separation zone is diminished leading to the primary separation line being closer to the model. The relation between the interaction and the shock system will be discussed in Chapters 4 and 5.

Strakes 2 and 3 have another unique oil flow feature. Looking at the oil

patterns of both cases, it is noticed that the oil streaks suddenly deflect in the area marked by a circle (see Figs.3-1(d) and (e)). This sudden deflection is believed to be caused by the fin shock reaching the wall and will be also discussed later.

3.2 Wall pressure distributions

Isobar patterns on the test surface are presented for each model in Fig.3-3. Fig.3-4 compares pressure distributions for the five cases obtained along the Y_s direction at $X_s=46\text{mm}$, 70mm, 93mm and 130mm. The locations where the distributions have been obtained are marked by the dotted lines and labelled a~d in Fig.3-3. The intervals of a-b and b-c are same and are about 24mm but c-d was chosen as 37mm in order to compare the distributions where the maximum pressure levels appeared and they occurred just upstream of the trailing edge of the model.

As observed in Fig.3-4, the five cases can be classified into two groups. The pressure distributions for the Unmodified, Fillet and Strake 1 geometries have a clear "plateau" zone, which is evidence of separation (It should be noted that the plateaus at $X_s=46\text{mm}$ are ambiguous. see Fig.3-4(a)). These plateau regions are recognized as an area whose isobars are located with wider intervals (see Figs.3-3 (a)~(c)). But Strakes 2 and 3 no longer have such well defined zones.

The pressure levels for Strakes 2 and 3 are lower than the first three cases until around position c. This may be due to the fact that the interaction is dominated by the weaker strake shock. But further downstream (see Fig.3-4(d)), the pressure levels recover to the unmodified level at a certain Y_s and finally exceed the unmodified level when they approach the trailing edge area. In both cases, the pressure gradient around the trailing edge of the model is steeper than the cases of the Unmodified and Strake 1 (see Figs.3-3 (d) and (e)) and this steeper gradient appears downstream of the sudden oil deflection observed in Figs.3-1(d) and (e). Hence it is reasonable to suppose that the fin-shock impingement increases the wall pressure when it reaches the wall and the surface

oil streaks are deflected by the pressure gradient. But information from the "footprint" data is limited to consider such complex behaviour. The relation between surface features and the shock system will be discussed in Chapter 4.

3.3 Liquid crystal thermal patterns

Liquid crystal (LC) thermal patterns on the wall for the cases of unmodified C, Strake 2C and Strake B2C are presented in Fig.3-5. Only in this figure, the flow direction was arranged from right to left because the positioning of the tunnel and the camera was opposite to the other experimental cases. (Reversed images were obtainable but the quality of the images deteriorated during the data processing from the original video-recorded data.) The general procedure for conducting the tests was first to provide an electric current with a certain voltage (about DC 15V) for the heater foil until a blue colour covered the whole heater surface. After the tunnel was started, the voltage was increased up to 50V to obtain the highest LC colour contrast.

In the case of the unmodified C model, two distinct lines are clearly seen in Fig.3-5(a) and are labelled L_1 and L_2 respectively. Based on the surface flow patterns obtained individually (not shown here), the location of the first line (L_1) corresponds to the primary separation line. On the other hand, a much more vivid line, L_2 , is close to the inviscid oblique shock location. The colour difference between the L_2 and the model is much larger than that between L_1 and L_2 . In the "hot wall" case, when the wall temperature is higher than the flow recovery temperature, an area with higher heat transfer tends to be cooled down more quickly. Hence it is deduced from Fig.3-5(a) that the heat transfer from the wall starts to rise at the primary separation line and is boosted when the flow reaches the inviscid shock location. This tendency is similar to the heat transfer behaviour observed in Ref.76. It should be noted that the surface temperature of the heater assembly tends to be cooled from its edges even when it is surrounded by the insulator. Hence the colour around the edges becomes brown more quickly than

the rest of the heater surface without relating to the interaction itself. A larger heater assembly may provide an adequate large area without the disturbance from the edges.

Once a strake is introduced, the LC colour difference is less distinct. In the case of Strake 2C, a slight colour difference still indicates the same qualitative behaviour as observed in Unmodified C but for Strake B2C there is no longer any perceivable colour difference (see Figs.3-5(b) and (c) respectively). This again suggests that the strake has a weakening effect on the interaction.

The LC method seems to be more attractive when the interaction becomes stronger with higher M_∞ and α_{fin} . More vivid LC colour differences can be observed under such circumstances because a stronger interaction leads to a stronger heat transfer. A quantitative analysis of LC thermographs is quite possible using a colour image analyzer but one was not available for the present study which is thought only qualitative.

3.4 Evaluation of each modification technique

The flow features obtained by oil flow visualization, the pressure measurements and liquid crystal thermography strongly suggest that a sensibly large strake which also covers the whole of the root chord can have the effect of weakening the flow separation (Strakes 2, 3, 2C and B2C belong to this type). Contrary to the effectiveness of these strakes, the fillet caused a steeper pressure gradient and a more vivid plateau region (see Figs.3-3(b) and 3-4), as well as causing a larger separation zone as indicated by the primary separation line (see Fig.3-2). These results show that separation was strengthened rather than weakened by the fillet used here.

The effect of Strake 1 was weak and limited to the vicinity of the strake. Surface oil streak patterns on the wall and on the model surface photographed individually are combined and displayed in Fig.3-6(a) for Strake 1. Because of the geometrical discontinuity along the joint between the strake and the fin (strake

joint), the strake was attached to the figure separately from the fin. But no real gap existed along the strake joint. The main flow features read from Fig.3-6(a) are sketched in Fig.3-6(b) and the proposed vortical structure corresponding to the oil flow pattern is shown in Fig.3-6(c).

Around the strake joint, a small but vivid separation, originating from the strake separation line and terminating at the strake reattachment line, is observed (see Figs.3-6(a), (b)). The separation vortex relating to this separation is named "Strake vortex" in Fig.3-6(c). The "joint streak" starts from the oil accumulation formed by the strake vortex but is not an oil convergence line except the vicinity of the strake as discussed in Sec.3.1. Other features, such as primary separation & reattachment, secondary separation & reattachment, corner separation and the corresponding vortical structure are similar to those observed in the unmodified case (compare Fig.3-6(c) with Fig.1-2).

Fig.3-7 shows surface isobar patterns and the main surface streak features read from the oil flow pattern appeared in Fig.3-6(a). The darker and the lighter shaded areas indicate a relatively higher ($\geq 102\%$) and a lower ($\leq 98\%$) pressure area than the pressure level of the unmodified case, respectively. The pressure levels outside these shaded areas are quite similar to the unmodified levels (within $\pm 2\%$). As observed in the figure, the area affected by Strake 1 is very small and is limited to the vicinity of the strake. The changes in the position and feature of the primary separation line are also restricted to the region around the strake. Looking at the joint streak position, its location is exactly overlapped on the isobar of $P/P_\infty = 1.45$ around where neighbouring surface streaks do not converge into the line but run in parallel. Pressure level and oil flow features downstream of the joint streak are almost the same as for the unmodified case except the zone adjacent to the strake joint. These observations suggest that the oil accumulation triggered by a compression at the strake joint flows downstream along the line with constant pressure and results in the thick surface streak line (joint streak).

CHAPTER 4 Flowfield structures

Based on the observations in Chapter 3, a type of strake which covers the whole of the root chord seems to have a weakening effect on separation. Here the apex of the strake is simply connected to the root trailing edge by a straight line. But the strake 1 type modifications did not have a satisfactory effect on the interaction and the fillet strengthened the interaction rather than weakened it. This chapter surveys the flowfield structures by means of the laser light sheet flow visualization (LFV) and schlieren photography in order to progress the physical understanding of the flowfield behaviour. The flows around the fin with effective-type of strakes are then calculated using an Euler CFD solver. The computed results and surface oil streak patterns are used in order to consider the effects of strake geometry on the flowfield structure. The information obtained here will provide quantitative data for the prediction method detailed in Chapter 5.

4.1 Flowfield behaviour deduced from LFV images

To understand the flowfield behaviour, a series of LFV tests were undertaken. In order to obtain satisfactory LFV images, water vapour had to be continuously injected into the tunnel settling chamber during the testing. If the vapour supply was interrupted, the flow picture "disappeared". The amount of water injected for each test was about 5 litres. Unfortunately this amount of moisture caused a condensation shock to form in the tunnel nozzle. Sec.4.1.1 will discuss this undesired phenomenon before going onto the investigation of the LFV images.

4.1.1 Condensation shock problem

When the humidity of water vapour reaches a certain level, a condensation shock occurs upstream of the test section. This shock causes a deviation from the isentropic expansion process and reduces the actual Mach number below the

Mach number for the "dry" condition. The details of this phenomenon are given in Refs.77 and 78.

To evaluate the actual Mach number in the test section, the isentropic relations between the total pressure measured in the settling chamber and the static pressure in the test section are no longer applicable because of the existence of the condensation shock. So when the tunnel was running "wet", the oblique shock angle measured from the LFV images was employed. Fig.4-2(a) exhibits several LFV images of the flow around the unmodified fin. The pictures show the flow through planes A~D as marked on the schlieren picture taken from the top to bottom direction of the model. By connecting the location of the straight part of the shock (labelled SPF in Fig.4-2(a)) for each LFV image, the inviscid shock location can be fixed. The solid circles in Fig.4-1 are the locations of the SPF read from each LFV image of Fig.4-2(a). The shock angles read from the schlieren image obtained at the dry condition (see the single-chain-dotted line in Fig.4-1) is 37.5° , which is equal to the theoretical oblique shock angle at $M_\infty=2.46$. However the angle of the line linking the solid circles, 43.9° , corresponds to $M_\infty=2.06$. The reduction of the Mach number was -16%. Comparing the two cases in terms of M_n which has a dominant role to determine the interaction regime (see Fig.1-8), $M_{n/moist}$ of 1.43 is 4.6% lower than $M_{n/dry}$ of 1.50. This reduction changes the interaction regime from the "incipient secondary separation" to the "primary separation" (corresponding to the conditions marked by solid circle and triangle of Fig.1-8 respectively). Hence the interactions observed by the LFV are weaker than those obtained by other techniques and flow behaviour relating to secondary separation can not be expected in the LFV images. But the change of the qualitative flow features is believed to be limited with the present deviation of M_n as deduced from Fig.1-8. It should be noted that the Mach number deterioration can be reduced by means of liquids which have a lower latent heat of evaporation than water, for example McGregor⁽⁷⁴⁾ used carbon tetrachloride (CCl_4).

4.1.2 LFV images and flowfield behaviour

Typical flow field images obtained by the LFV technique are shown in Fig.4-2 for the five cases together with the schlieren images. The bright vertical and horizontal lines in the LFV images are laser light reflections from the model and from the tunnel wall respectively. As mentioned in Chapter 2, the model and the tunnel wall were painted matt black to reduce the laser reflections but a certain level of reflection was inevitable. Three-dimensional shock structures deduced from the LFV images are illustrated in Fig.4-3. It should be noted that the laser sheet direction was arranged to be perpendicular to the unmodified inviscid shock direction at the dry air condition. However the sheet direction had an angle of about 84° to the actual shock direction because of the Mach number deterioration mentioned in Sec.4.1, but this does not affect the following discussion.

The unmodified fin created a clear λ -shock structure resulting from the intersection of the separation shock and the fin shock (see Figs.4-2(a),4-3(a)). The fin shock above the triple point is slightly curved in the leftward direction of the LFV images. In the schlieren picture, the locations of the triple point and the straight part of the fin shock can be identified as the front and back of the broad black band and labelled "TP" and "SPF" respectively on the picture. Beneath the λ foot, the core of the separation zone appears as a dark area in the LFV images.

The fillet creates the same shock structure as the unmodified case (see Figs.4-2(b),4-3(b)). But the size of the λ foot is slightly larger than the unmodified one. It appears that the fillet strengthens separation rather than weakening it. The shock angles read from the SPF position of the schlieren picture (taken at the "dry" condition) is about 40° , which results in $M_n=1.58$, a 5.6% higher value than that of the unmodified fin at the dry condition. The solid square in Fig.1-8 indicates that the fillet may have stronger secondary separation than incipient secondary separation observed in the unmodified case. However a clear difference could not be detected from the oil flow features (see Fig.3-1(b)) and from the LFV images except the size of the interaction zone.

In the case of Strake 3 (Figs.4-2(e),4-3(e)), the strake shock intersects the fin shock, and bends it strongly towards the model. The lower-edge positions of the strake- and fin-shocks can be identified as two weak black lines in the schlieren image. They are labelled LES and LEF respectively in Figs.4-2,4-3(e). Between the two lines, the fin shock above the intersecting point appears as a relatively broad black belt in the schlieren image. It is very hard to see any λ -shock structures caused by the separation shock at the LES and LEF and neither can a clear core of the separation zone be distinguished in the LFV images. It is obvious that Strake 3 has a weakening effect on separation.

Strake 2 (Figs.4-2(d),4-3(d)) has a similar shock structure as for the Strake 3 case. However the strake shock appears more vivid in the schlieren image (labelled LES in Fig.4-2(d)), whilst the fin shock behind the strake shock is very faint in the LFV images. It is reasonable to suppose from the 2-dimensional double wedge with fixed total turning angle that the fin shock behind the strake shock becomes strong with weakening of the strake shock and vice versa.

Strake 1 (Figs.4-2(c),4-3(c)) generates a small, weak strake shock, which can not be detected in the schlieren image. The fin shock behind the strake shock seems to be strong but the fin shock is also bent by the strake shock in the same manner as Strakes 2 and 3. After the fin shock reaches the wall boundary layer, a λ -shock structure caused by a separation shock develops and can be clearly seen with the separation core in the C and D positions of the LFV images.

Based on the LFV and schlieren observations, it seems reasonable to conclude that separation can not be prevented or weakened by junction modification techniques unless the shock system is weakened. In addition, the flow is thought to be steady by comparing a number of schlieren and LFV images.

4.2 Numerically-obtained inviscid flow structures

As observed in Sec.4.1.2, the inviscid shock structures have important roles

in specifying the interaction strength. Hence computational analyses using the Euler equation were introduced in order to help understand the inviscid flow structures. An Euler CFD code developed by Griesel⁽⁷⁹⁾ was applied for the present configurations and three dimensional inviscid flowfields were obtained computationally for Strakes 2, 3, B1 and B2. A brief outline of Griesel's code is provided in Appendix B.

4.2.1 Code validation

Griesel's code has already been well validated two-dimensionally using a wedge in a duct, a backward-facing ramp and a biconvex airfoil⁽⁷⁹⁾. Three-dimensional validation using a cone at various incidences has also been undertaken⁽⁸⁰⁾. In addition, computed shock patterns have been compared with the actual patterns read from the LFV images in order to check the validity for the current applications. Fig.4-4 presents some typical LFV images and corresponding computational results for Strakes 2 and 3 obtained at the locations of A~D in Figs.4-2(d) and (e). Because the brightness of a flow feature in the LFV images is considered to be proportional to the air density, the computed static density contours, whose levels (ρ/ρ_∞) are indicated by the colour ranges in Fig.4-4(c), are employed for the comparison. It should be also noted that the LFV images were captured at $M_\infty=2.06$ because of the condensation shock discussed at Sec.4.1.1, hence the comparison was also carried out at this Mach number. Even though the Euler solver treated inviscid behaviour only, qualitative shock features were correctly calculated as can be observed from Fig.4-4.

Fig.4-5 compares the experimental and computational shock shapes read from Fig.4-4. The position of the shock system and the shape of the fin shock including the part behind the strake shock were predicted quite precisely. Griesel's CFD solver is considered to be valid enough to investigate the inviscid shock structures of the current applications.

It should be noted that the strake shock interacts with the wall boundary layer and tends to deviate from its inviscid shape. On the other hand, the fin

shock, which is not affected by the viscous force until it reaches the boundary layer, has a good agreement with the inviscid calculation. The interaction influenced the shape of the shock system up to the intersection point between the strake- and the fin shocks and the mismatch was amplified with the distance from the leading edge.

Another difference is that the computed fin shock clearly attaches to the model surface whilst one can not observe the fin-shock attachment in the LFV images because the strong laser reflection prevents observation around the model and the bent fin shock itself is very weak (especially in the case of Strake 2, the bent fin shock is very faint in the LFV images, see Figs.4-2(d) and 4-4(a)). In the experimental case, the terminal location of the bent fin-shock observed from the LFV images was called its lower edge. As to the computed fin shock, the lower "edge" does not exist because the shock attaches to the model. But in the following discussion, the term "lower edge" will be used in order to express the part of the computed fin shock adjacent to the model surface.

4.2.2 Inviscid flow structures

Static pressure contours at several Y-Z slices (whose positions are identified by numbers) are shown in Fig.4-6 for each strake at $M_\infty=2.46$. For the following discussion, pressure rises across the shock system will be important. Hence the static pressure contours were employed to display the flowfield instead of the density contours used for the code validation. For each slice, the position of the top of the model is indicated by a triangle. Pressure contours on the wall and on the model are also attached to the figure with the numbers corresponding to the number of each Y-Z slice. Pressure levels (P/P_∞) are identified according to the colour ranges displayed in Fig.4-6(e). The inviscid flow structures are clearly shown in the figure.

As long as the strake shock attaches to the strake leading edge, the inviscid shock structure is basically the same as those deduced from the LFV images except the vicinity of the wall and the model surface. The shock structure is

illustrated three dimensionally in Fig.4-7(a). As mentioned in the previous section, the inviscid fin shock (lighter-shaded part of the shock system) gradually approaches the wall with attaching to the model surface. The attached fin shock increases the pressure level on the model surface, which is observed as a colour change just below the junction of the strake and the fin (strake joint) (see the contours on the model in Fig.4-6). The fin shock then impinges on the wall and reflects from the wall (this reflection is not illustrated in Fig.4-7) so causing a very high-pressure zone around the trailing edge (see the bright-colour zone on the wall in Fig.4-6). Strakes 2 and 3 correspond to this case. Under the real conditions, the fin-shock reflection from the wall is modified by the interaction with the wall boundary layer and is not detected by the LFV images. But the reflection may have been responsible for the high pressure rise observed close to the trailing edge for the fins with Strakes 2 and 3 (see Figs.3-3(d),(e) and 3-4(d)).

When the sweep angle of the strake increases, the strake shock detaches from the strake leading edge. The inviscid shock structure with the detached strake shock is deduced from Fig.4-6(d) and is interpreted in Fig.4-7(b). The detached strake shock (see the darker-shaded part of the shock system in Fig.4-7(b)) encounters the fin leading edge at the point B above the strake joint labelled A. Behind the detached strake shock, the Mach number is less than M_∞ and the fin-shock angle increases from the angle based on M_∞ , so that the part of the fin shock originating from the line A-B is deformed (see Fig.4-7(b)). Downstream of the deformed fin-shock, a high pressure region is created since the flow has passed through both the detached strake shock and the deformed fin shock. This high-pressure zone can be seen as a bright-coloured area in the No.1 and 2 Y-Z slices and on the model surface of Strake B2 (see Fig.4-6(d)). Strake B1 also has a similar tendency (see Fig.4-6(c)).

In order to judge whether a strake is accompanied by an attached shock or a detached shock at a certain M_∞ , the stream deflection angle in the direction normal to the strake leading edge is calculated by the oblique shock theory. If the deflection angle is smaller than the detachment level, the strake shock attaches to

the leading edge and vice versa. Settles and Kimmel⁽¹⁵⁾ approximated this criterion by defining a "detachment similarity parameter" as

$$\zeta = \frac{\alpha_{nle} + 38.53}{M_{nle}(1 - 0.149M_{nle})} \quad (4-1)$$

where α_{nle} and M_{nle} are the strake half-apex angle in degrees and the Mach number component normal to the strake leading edge respectively (see Fig.4-8) and they are given by

$$M_{nle} = M_{\infty} \cos \lambda$$

$$\alpha_{nle} = \tan^{-1} \frac{\tan \alpha_{str}}{\cos \lambda} .$$

Settles and Kimmel approximated ζ as 43.6 for the shock detachment at supersonic Mach numbers up to 3.

The values of ζ for the strakes were calculated by eq.(4-1) and are presented in Table 4-1 with corresponding values of M_{nle} and α_{nle} . Computed static isobar patterns upstream of the fin-leading edge are also displayed in Fig.4-9 for each strake. As observed from the table and the figure, the CFD code quite correctly predicted whether the shock was attached or detached for all cases calculated. For Strakes 2 and 3, their conditions are far from the detachment criterion expressed by ζ of 43.6 and the strake shock completely attaches to the leading edge at $M_{\infty}=2.46$ (see Figs.4-9(a) and (b)). Under such circumstances, an area of constant pressure appears adjacent to the strake-leading edge, over which the shock wave has a straight shape. This phenomenon is a typical feature of a swept wing with supersonic leading edges. On the other hand, Strake B2 clearly has a detached strake shock (see Fig.4-9(d)). In this case, the constant-pressure area no longer exists and the strake shock is mildly curved all over the flowfield. As the value of ζ suggests, Strake B1 is quite close to the detachment criterion at this Mach number and flow features observed in Fig.4-9(c) are similar to the Strake B2. In this case, the strake shock is believed to be slightly detached from the leading

edge. Hence its flow structure will be approaching the type illustrated in Fig.4-7(b).

In addition to the global qualitative features of the inviscid flow field, some useful quantitative information can be read from Fig.4-6. Firstly, the relation between the strength of the strake shock and the strake geometry can be observed. One may find a way to estimate the strength of the strake shock for other strake geometries using this relation. The second piece of information concerns the way in which the fin shock propagates toward the wall. The strake height, required to prevent the fin shock from reaching the wall until the trailing edge of the model, can be calculated from the results shown in Fig.4-6. The relationship linking the strength of the strake shock to the fin shock behind the strake shock is another important piece of information. As already mentioned, the lower edge of the fin shock (LEF) becomes strong when the strake shock is weakened and vice versa. In the case of a relatively strong strake shock (see Fig.4-6(a)), the pressure rise across the LEF is small. On the other hand, the LEF has a much larger pressure rise when the strake shock becomes weak (see Fig.4-6(c) and (d)).

These factors will be employed in the next chapter to find the strake geometries required to prevent separation.

4.3 Oil flow patterns and vortical flow structures

In addition to the shock wave system discussed in Sections 4.1 and 4.2, the vortical flow features need to be understood. Unfortunately the LFV images shown in Fig.4-2 are not clear enough to present them vividly and Griesel's CFD code can not handle the vortical system related to the shock wave/boundary layer interaction. Hence the surface oil flow patterns are used in order to deduce the vortical-flow structure.

Further oil flow tests were conducted for the strakes of group B and the results are presented in Fig.4-10. The cases of Strakes 2 and 3 are also shown. In order to provide the relation between the surface streak features and the inviscid

shock structure, the locations of the strake shock and the fin-shock impingement read from Fig.4-6 are presented by the solid lines in Fig.4-10. (The solid line is not marked in Fig.4-10(e) because no CFD calculation has been carried out for this case.) The surface flow patterns taken from Fig.4-10 are sketched in Fig.4-11 and the proposed vortical structure corresponding to the flow patterns is shown in Fig.4-12(a). All lines of convergence and divergence are labelled C_{1-4} and D_{1-3} in these figures.

The lines of convergence and divergence labelled C_1 and D_1 are caused by the strake shock and they are a separation line and a reattachment line of the primary vortex PV, respectively (see Fig.4-12(a)). When the strake shock is still strong enough to cause separation (corresponding to the cases of Strakes 2 and 3, see Figs.4-10(a) and (b)), a clear oil convergence marked C_1 is created well upstream of the inviscid strake-shock location. Under such circumstances, the primary vortex is considered to be created in a similar way to the unmodified primary separation, which has been studied by many investigators as discussed in Chapter 1. From the numerical studies by Knight et al.⁽³³⁻³⁵⁾, it has been shown that some of the stream lines in Surface 1 leave the wall along the separation line C_1 (see Fig.4-12(a)). Another group of stream lines in the surface denoted as Surface 2 originate in the incoming flow and impinge on the wall at D_1 (see the single-chain-dotted surface in Fig.4-12(a)). The fluid contained between the wall and Surface 2 spirals into the vortical structure labelled PV. A small separation at the corner of the wall and model, detected by the convergent line C_4 , corresponds to the corner vortex observed for the unmodified fin⁽¹²⁾ (see Fig.1-2) and is marked by CV in Fig.4-12.

A unique vortical structure related to the strake flowfield is caused by a separation along the strake joint (the junction of the strake and the fin). This separation is shown along Surface 3 marked by the double-chain-dotted surface in Fig.4-12. The separation originates at the line of convergence C_2 and terminates at the line of divergence D_2 . The incoming flow is attached along the strake surface but then encounters the adverse pressure gradient existing around the

strake joint (see the contours on the model in Fig.4-6). It is noticed from Fig.4-6 that this pressure rise is mainly caused by the fin shock attachment to the model surface but a corner formed by the strake joint also increases the pressure gradient to a certain extent (The pressure rise by the strake joint is identified as a small yellow zone around the joint. See, for example, No.3~5 Y-Z slices of Strake B1 in Fig.4-6(c)). The Surface 3 separates along C_2 and forms a separation vortex named "Strake-joint vortex (SJV)" in Fig.4-12. When the SJV approaches the corner vortex (CV), C_2 merges with C_4 and, in the cases of Strakes 2 and 3, another separation line marked C_3 is observed between C_4 and D_2 . These phenomena suggest that SJV runs into the corner vortex and a reversed flow travelling downward from D_2 reseparates at C_3 . After C_2 meets C_4 , the streamlines feeding the vortex SJV no longer originate from the strake surface flow but come directly from the flow along the wall. Weak separation originating from C_3 forms "Second strake-joint vortex (SJV2)" (see Fig.4-12) and soon reattaches to the model surface. The reattachment line, D_3 , is so close to the line of C_3 that they seem to be a single line in the oil flow patterns (see the line labelled C_3+D_3 in Figs.4-10 (a),(b) and 4-11).

When the strake shock is weakened by increasing the strake sweep angle (λ) and decreasing the half apex angle (α_{str}) (see Figs.4-10(c)~(e)), the oil convergence line C_1 approaches the inviscid strake shock location, gradually becomes more diffuse and, in the case of Strake B2, finally disappears. Under such circumstances, the vortical system related to the primary vortex tends to disappear. However as the strake shock weakens, the fin shock strengthens behind the strake shock and may become strong enough to cause separation. As a result, the vortical structure illustrated in Fig.4-12(b) can develop. There is no primary vortex (PV) but when the fin shock finally reaches the wall it causes separation at CF and a vortex FV is formed. Strakes B1 and B2 have a vivid oil flow coalescence marked by CF in Figs.4-10(c) and (d). By comparing the positions of CF and the fin-shock impingement in Figs.4-10(c) and (d), it is clear that the oil flow coalescence CF is induced by the fin-shock impingement and separation

may occur along the coalescence line.

For Strake B3, there is no vivid oil flow coalescence on the wall except the vicinity of the strake apex (see Fig.4-10(e)). In this case, the effects of both the strake shock and the fin shock are believed to be weakened by the strake, so that no strong vortical separation is expected on the wall.

The observations discussed here have increased the understanding of the relationship between the shock system and the observed oil flow patterns. The next chapter will concern the prediction the shape of the strakes required to prevent separation on the wall based upon the information contained in this chapter.

CHAPTER 5 Prediction of the necessary geometric requirements for the strake needed to prevent separation on the wall

5.1 Basic conditions to prevent separation on the wall

The shock structures surrounding the fins with several strakes observed in Chapter 4 show the way in which the fin shock propagates towards the wall. The height of Strakes 2, 3 and B1 prevented the fin shock from reaching the wall until near the trailing edge. However the trailing-edge area was still affected by the fin shock, hence a slightly taller strake might prevent the fin-shock effect completely. When the strake is lower (e.g. Strakes 1 and B2), the fin shock quickly reached the wall boundary layer and caused boundary layer separation.

If the height of the strake is tall enough to prevent the fin-shock effect, the flowfield is controlled by the strake shock instead of the fin shock. In that case, the strength of separation solely depends on the strength of the strake shock. Comparing Strakes 2 and 3 for example, the longer strake (Strake 3) generated a weaker shock because of its relatively smaller apex angle (α_{str}) and larger sweep angle (λ). Hence the effect of Strake 3 on separation was greater than the effect of Strake 2. If the strake shock could be further weakened whilst maintaining the strake height to prevent the fin-shock effect, separation would be suppressed completely on the wall (Strake B3 may correspond to this case).

These observations suggest that a strake must have a geometry which satisfies the following conditions to prevent separation on the wall.

- 1) A strake must be long enough to weaken the strake shock, (i.e. must have a small apex angle and a large sweep angle).
- 2) A strake must be tall enough to prevent the fin shock from reaching the wall until the trailing edge of the model.

As discussed in Chapter 4, the lower edge of the fin shock is weakened with the strengthening of the strake shock. If both the strake shock and the fin shock are so weak that neither induces separation then the fin shock can be allowed to reach the wall ahead of the trailing edge. In that case, separation can be prevented by a lower height than predicted from condition (2). But it seems to be very difficult to predict this particular situation because of the complex three-dimensional flow structure. Hence condition (2), which prevents the effect of the fin shock completely regardless of its strength, was introduced in this study.

Based on conditions (1) and (2), the necessary geometrical requirements for a strake in terms of L_{req} and H_{req} (minimum length and height of the strakes required to prevent separation) are predicted for the present experimental condition (i.e. $M_\infty=2.46$, $\alpha_{fin}=15^\circ$). The prediction method is then generalized for other α_{fin} and flow conditions. Fig.5-1 shows the notation used in this chapter.

If the required height is higher than or almost equal to the height of the fin considered, separation can not be prevented by a strake. In that case, another technique must be introduced (e.g. a completely swept fin).

5.2 Estimation of the strake-shock strength for arbitrary strake geometry and Mach number

In order to consider condition(1), the strength of the strake shock must be predicted for arbitrary strake geometries and Mach numbers. To evaluate the strength, flowfields around a series of rhombic delta wings (RDWs) were calculated by the Euler CFD solver employed in Sec.4.2 because the RDWs generate inviscid shock waves appropriate to the strake shocks (see Fig.5-2). (In this chapter, the suffix "str" is also used for the RDW for convenience).

The CFD calculations were carried out for $30^\circ \leq \lambda \leq 60^\circ$ and $8^\circ \leq \alpha_{str} \leq 17^\circ (\leq 14^\circ \text{ at } M_\infty=2.0)$ at $M_\infty=2.0, 2.46$ and 3.5 . This range was chosen to include the present experimental situation with a wider band of supersonic Mach numbers under which the shock wave remains attached to the delta wing apex. A few

examples of the calculated flowfields are displayed in Fig.5-3 for $8^\circ \leq \alpha_{\text{str}} \leq 14^\circ$ and $30^\circ \leq \lambda \leq 60^\circ$ at $M_\infty = 2.46$. The figures show static pressure contours in the plane of symmetry of the delta wing (see Fig.5-2). The pressure levels (P/P_∞) are explained in the colour bars attached to the figure. In Fig.5-3, the conical nature of the flow has been correctly calculated and one can see not only the inviscid static pressure levels but also the shock-wave angles very clearly.

In order to provide the validation data for the CFD results, experiments were conducted using the six different rhombic delta wings shown in Fig.5-4. To avoid the tunnel choking, they were designed with blockage ratios less than 7.5% of the tunnel cross section. Each wing was mounted in the 2.5"×2.5" tunnel by a sting with its apex on the tunnel centre line (see Fig.2-2). Shock patterns caused by the delta wings have been obtained by schlieren photography and are presented in Fig.5-5. Because of the small size of the tunnel, the pictures had to be enlarged whilst the quality of the images deteriorated in the printing process. But they are clear enough to read the shock angle. The vertical bars in Fig.5-6 indicate the shock angles read from the images. At least five distinct pictures obtained at different moments were used to determine the shock angles for each case. Reading and repeatability errors of a few percent were inevitable, hence the test results are expressed by the vertical bars. The solid circles linked by single-chain-dotted lines are CFD results read from Fig.5-3. They are in excellent agreement with the experimental shock angles. Griesel's code seems to be valid also for this application.

The strength of the strake shock was evaluated from the computed static pressure distributions in the plane of symmetry of the rhombic delta wings because the inviscid shock strength in the vicinity of the wall boundary layer was considered to be of primary importance in specifying the interaction⁽¹³⁾. Fig.5-7 shows a schematic three-dimensional view of the calculated two-dimensional static pressure distribution in the plane of symmetry. To specify the shock strength by a single pressure ratio, the pressure distribution was averaged using the construction shown in Fig.5-7 and the averaged pressure ratio was termed

"equivalent pressure ratio (PR_{eq})".

Fig.5-8(a) illustrates two types of pressure distributions (obtained along the Y-axis shown in the figure) caused by a two-dimensional wedge and a rhombic delta wing with a same half-apex(wedge) angle (i.e. $\alpha=\alpha_{str}$). As can be seen from the figure, the level of PR_{eq} was larger than the pressure level just behind the strake shock but was smaller than the two-dimensional oblique shock pressure ratio labelled PR_{os} in Fig.5-8(a). At an early stage, the strength of the strake shock was evaluated by the normal shock relations using the computed strake shock angle and the Mach number component normal to the shock wave (see PR_{ns} in Fig.5-8(a)). It can be seen that PR_{ns} is much closer to the pressure level just behind the strake shock. But in the case of the rhombic wings, the pressure behind the strake shock increases gradually towards the wing surface because of the conical nature of the flow (see the shaded part in Fig.5-8). When the shock angle is same (see Fig.5-8(b)), the pressure jump across the shock is almost same in the vicinity of the plane of symmetry regardless of the geometry of shock generators (i.e. $PR_{ns}\approx PR_{os}$ in Fig.5-8(b)). But shock waves with equal shock angles do not necessarily produce equivalent interactions. For example, a swept sharp fin tested by Fomison⁽⁴²⁾ produced a clear separation at a condition of $\lambda=75^\circ$, $\alpha=13^\circ$ and $M_\infty=2.4$, whilst the shock wave caused by the unswept sharp fin with $\alpha=5.5^\circ$, whose angle was same as that of the swept fin, was not strong enough to cause separation. This fact infers that the overall pressure rise, rather than the pressure jump across the shock, has a dominant role in specifying the interaction behaviour. Hence PR_{eq} was finally employed to evaluate the effect of strake-shock's strength, because it represented the whole of the pressure increase in the plane of symmetry. Note that the term "strake-shock strength" is employed to refer to PR_{eq} in the following discussion but it does not equal to the pressure jump across the shock in most of the cases. The solid circles in Fig.5-9 show the computed values of PR_{eq} obtained at various α_{str} , λ and M_∞ . The value at $\lambda=0^\circ$ (corresponding to the value of the 2-D oblique shock) is also marked for each case as a reference.

In order to predict the strake-shock strength for arbitrary α_{str} , λ and M_∞ , PR_{eq} must be correlated with them. To construct the correlation law, the angle between the plane of symmetry and the face of the delta wing,

$$\xi = \tan^{-1} \frac{1}{\sin \alpha_{str} \tan \lambda} \quad (5-1)$$

(see Fig.5-10) was introduced. When α_{str} and λ increase, ξ tends to depart from 90° (see Fig.5-10(b)). Similarly PR_{eq} will decrease from its value at $\lambda=0^\circ$ (i.e. PR_{os} in Fig.5-8(a), see also the values at $\lambda=0^\circ$ in Fig.5-9) as ξ drops below 90° . Hence a simple correlation using ξ and PR_{os} was first introduced but this did not collapse the data sufficiently as shown in Fig.5-11(a). In order to improve the correlation, the data must be plotted by introducing a minor role of M_∞ whilst considering the condition that PR_{eq}/PR_{os} has to approach 1.0 when ξ approaches 90° . The relationship

$$\frac{PR_{eq}}{PR_{os}} \frac{2\xi}{\pi} + 0.05 |M_\infty - 2.5| \left(\frac{\pi}{2} - \xi \right) \quad (5-2)$$

was finally introduced, where ξ is expressed in radians. This plot greatly improved the data correlation with ξ (see Fig.5-11(b)). Thus the value of PR_{eq} is determined directly from

$$PR_{eq} = \frac{\pi PR_{os} \{ F(\xi) - 0.05 |M_\infty - 2.5| \left(\frac{\pi}{2} - \xi \right) \}}{2\xi} \quad (5-3)$$

where $F(\xi)$ was obtained by the least squares method as

$$F(\xi) = A_0 + A_1 \xi + A_2 \xi^2 + A_3 \xi^3 + A_4 \xi^4$$

$$\begin{pmatrix} A_0 \\ A_1 \\ A_2 \\ A_3 \\ A_4 \end{pmatrix} = \begin{pmatrix} -0.8808 \\ 2.0571 \\ -0.6355 \\ -0.4630 \\ 0.3277 \end{pmatrix} .$$

The values of PR_{eq} from the CFD calculations and eq.(5-3) are compared in Fig.5-9 at various M_∞ , α_{str} and λ . The agreement is very good.

Some extreme cases were calculated in order to estimate the effective range of eq.(5-3) (see the cases marked by a solid circle, a solid triangle, a solid box and a cross in Fig.5-11(b)). Based on these data, eq.(5-3) has been estimated to be quite accurate (within 5% of the corresponding CFD value) for the range $2.0 \leq M_\infty \leq 5.0$, $0^\circ \leq \alpha_{str} \leq 20^\circ$ and $0^\circ \leq \lambda \leq 70^\circ$. Out of this range, the accuracy deteriorates with M_∞ , α_{str} and λ . In terms of eq.(5-3), PR_{eq} can be easily specified for arbitrary α_{str} , λ and M_∞ within the effective range of eq.(5-3).

In a similar way, the values of the calculated strake-shock angle, β_{str} , can also be correlated with the strake geometries and M_∞ . This correlation law is useful for the following discussion and may have some intrinsic value in itself. Hence the correlation for β_{str} is presented in Appendix C with details of how the correlation has been obtained.

5.3 The strake height required to prevent the fin-shock effect

In order to consider condition(2), the propagation angle of the lower edge of the fin shock (LEF), η (see Fig.5-12), must be estimated. The most direct way of doing this is to trace the edge from the LFV images (see Fig.4-2). Unfortunately the LFV images are not clear enough to distinguish the terminal location of the fin shock. Instead of the LFV images, the prediction law of η was constructed from the computed data discussed at Sec.4.2.2. The minimum strake height required to prevent the fin-shock effect (H_{req}) is then predicted by the value of η .

From the inviscid flow structures shown in Figs.4-4 and 4-6, it can be seen how the LEF approaches the wall. The positions of the LEF (expressed as Z_{LEF} in Fig.5-1(b)) have been read from Fig.4-6 and are plotted by X_f - Z coordinate in Fig.5-13. (Hence the position of LEF is projected onto the X_f - Z surface (the side face of the unmodified part of the model) in order to specify the value of η , see Fig.5-12). In all cases the LEF approaches the wall linearly with X_f . It should be noted that the attachment/detachment of the strake shock discussed in Fig.4-7 does not affect this linearity. In order to specify the propagation angle, the shock locations were connected with the point of H_{str} (marked by a solid triangle on the ordinate) using a single-chain-dotted line (see Fig.5-13). This line provides us with a concrete propagation angle for each case. The third column of Table 5-1 shows the values of η read from Fig.5-13 at the present experimental condition with $M_\infty=2.46$ and $\alpha_{fin}=15^\circ$. Table 5-1 also contains some results at $M_\infty=2.06$, which were calculated for the CFD code validation data shown in Fig.4-4.

If the strake shock is treated as a two-dimensional oblique shock and PR_{eq} is introduced as the pressure rise across the shock, an "equivalent" Mach number in zone 1 (see Fig.5-1(b)), M_1 , can be calculated in terms of the oblique shock theory, i.e.,

$$M_1 = \sqrt{\frac{M_\infty^2 (6 PR_{eq} + 1) - 5 (PR_{eq}^2 - 1)}{PR_{eq} (PR_{eq} + 6)}} \quad (5-4)$$

The Mach angles based on M_1 can then be calculated by $\mu_1 = \sin^{-1}(1/M_1)$ (see the fourth column in Table 5-1) and are compared with the propagation angles measured from the CFD results. As can be seen from the table, the difference between the two values is quite small in every case, hence the Mach angle at M_1 was introduced to predict the values of η ;

$$\eta = \sin^{-1} \frac{1}{M_1} \quad (5-5)$$

Based on the assumption that the fin-shock impingement point is the intersection of the LEF and δ^* , the fin-shock impingement length (L_{imp}) is

determined by means of the value of η from eq.(5-5) and the geometrical relation illustrated in Fig.5-12 as

$$\frac{L_{imp}}{L_{fin}} = \frac{1}{\tan\eta} \left(\frac{H_{str}}{L_{fin}} - \frac{\delta^*}{L_{fin}} \right) . \quad (5-6)$$

The values of L_{imp}/L_{fin} are then calculated for the present experimental condition of $M_\infty=2.46$, $\alpha_{fin}=15^\circ$ and $\delta^*/L_{fin}=0.0273$ and are presented against L_{str}/L_{fin} and H_{str}/L_{fin} using a group of iso- L_{imp}/L_{fin} lines (see Fig.5-14). The zone where the strake sweep angle, λ , exceeds 70° is not shown in Fig.5-14 because this zone is thought to be beyond the effective range of eq.(5-3) (see the discussion in Sec.5.2) on which L_{imp} depends. The area of $H_{str}/L_{fin} \leq \delta^*/L_{fin}$ is also omitted because a negative value of L_{imp}/L_{fin} is meaningless. The geometries of all the strakes tested are marked by their names on Fig.5-14 in order to identify the level of L_{imp} for each strake.

From the computed inviscid-shock structure, it is realized that the fin shock first reaches δ^* above a point along the strake-wall junction (labelled A in Figs.5-12(b) and (c)). Once L_{imp} is longer than $L_{fin}/\cos\alpha_{fin}$, the expansion fan from the model trailing edge weakens the impinged fin shock and "quenches" further interaction (see Fig.5-12(c)). Hence

$$\frac{L_{imp}}{L_{fin}} = \frac{1}{\cos\alpha_{fin}} \quad (5-7)$$

can be treated as a criterion for the required strake height. This line of $L_{imp}/L_{fin}=1.035$ at $\alpha_{fin}=15^\circ$ is marked by a single-chain-dotted line in Fig.5-14. To prevent the fin-shock effect, a strake must have dimensions above this line.

5.4 Necessary geometric requirements for the strake under the present experimental condition

From the geometrical relations illustrated in Fig.5-1(a), α_{str} and λ can be expressed as

$$\alpha_{str} = \tan^{-1} \frac{L_{fin} \tan \alpha_{fin}}{L_{str} + L_{fin}} = \tan^{-1} \frac{\tan \alpha_{fin}}{\frac{L_{str}}{L_{fin}} + 1} \quad (5-8)$$

$$\lambda = \frac{\pi}{2} \text{ (or } 90^\circ \text{ in deg.)} - \tan^{-1} \frac{H_{str}}{L_{str}} \quad (5-9)$$

By means of eqs.(5-1),(5-3),(5-8) and (5-9), the values of PR_{eq} are obtained over a wide range of L_{str}/L_{fin} and H_{str}/L_{fin} for the present experimental case of $M_\infty=2.46$ and $\alpha_{fin}=15^\circ$. They are presented as a group of iso- PR_{eq} lines in Fig.5-15. The zone where λ exceeds 70° is not displayed in Fig.5-15 because of the ineffective range of eq.(5-3). The area around the origin ($L_{str}/L_{fin}, H_{str}/L_{fin}$)=(0,0) is also omitted because the origin is a singular point of λ so that the iso- PR_{eq} lines around this point are meaningless.

Line A in Fig.5-15 corresponds to the pressure level for incipient primary separation (IPS) due to an unswept sharp fin. This value of $PR_{IPS}=1.55$ is obtained from Korkegi's criterion (eq.(1-2)) with $M_\infty=2.46$. Below this pressure level, the shock is not considered to be strong enough to cause separation. Even though some uncertainty exists about this criterion as discussed at Sec.1.1.1, the pressure level from eq.(1-2) is taken as the criterion for the incipient primary separation in the present case. It should be noted that the term "separation" means significant vortical separation in this chapter and the effect of Re on the position of line A is ignored, as discussed at Sec.1.1.1.

In Fig.5-15, the line expressed by eqs.(5-6) and (5-7) for $\delta^*/L_{fin}=0.0273$ (i.e. the single-chain-dotted line in Fig.5-14) is also marked as line B. In order to prevent separation, a strake must have dimensions appropriate to the zone to the right of line A and above line B. This zone is hatched in Fig.5-15 with the label of "no separation". The minimum required length and height of the strake (expressed by L_{req} and H_{req}) can be read from the intersection point of the lines A and B (labelled R). They are $0.53L_{fin}$ and $0.57L_{fin}$ respectively.

The geometries of all strakes are marked in Fig.5-15 in order to identify the

level of PR_{eq} for each strake. As can be understood from the figure, Strake B3 was chosen from the "no separation" zone. Other strakes were selected from a wide range of conditions whilst maintaining a realistic shape of the strake against the fin.

5.5 Plausibility of the prediction

In order to consider the plausibility of the prediction, the surface oil flow patterns for Strakes 2, 3, B1, B2 and B3 (discussed in Sec.4.3) are used again and are re-printed in Fig.5-16. The corresponding values of PR_{eq} and L_{imp}/L_{fin} read from Figs.5-15 and 5-14 (calculated by eqs.(5-3) and (5-6) respectively) are printed in the figure. The locations of the strake shock and the fin-shock impingement, read from the CFD calculation, are also marked by the solid lines. (For Strake B3, the strake-shock location predicted by the β_{str} correlation law (see Appendix C) is marked because no CFD calculation was carried out for the case.)

When the strake-shock strength is above the level of line A (i.e. $PR_{eq} \geq 1.55$). Strakes 2 and 3 belong to this situation, see Figs.5-16(a),(b)), the oil convergence line caused by the strake shock (labelled C_1) is still clear and the primary vortex labelled PV in Fig.4-12(a) still survives. But the oil convergence line is weakened and approaches the inviscid strake shock location with reduction of PR_{eq} . Once PR_{eq} becomes less than the level of line A (see Strakes B1~B3), the oil coalescence C_1 loses its completeness and disappears in the case of the most weak strake shock (Strake B2, see Fig.5-16(d)). It should be noted that the clear oil convergence C_1 whose local deflection angle exceeds the inviscid strake-shock angle is still observed adjacent to the strake apex in the cases of Strakes B1 and B3 even when PR_{eq} is below 1.55 (see Figs.5-16(c) and (e)). This may correspond to the very weak separation observed in, for example, Refs.25, 41 and 42 but it is so small and weak that there is no practical importance for engineering design.

On the other hand, the oil-streak deflection caused by the fin-shock impingement becomes strong with weakening PR_{eq} (see the areas surrounded by

circles in Fig.5-16). The deflection is very faint in the cases of Strakes 2 and 3 but leads to a very vivid oil convergence observed for Strakes B1 and B2 (see the convergence marked CF in Figs.5-16(c) and (d)). This is because the strength of the lower edge of the fin shock (LEF) is intensified by weakening PR_{eq} . The pressure ratio across the LEF, PR_{LEF} , has been read from Fig.4-6 and is also printed in Fig.5-16 for each strake (except Strake B3). It is clear that the oil deflection is enhanced with PR_{LEF} . Strake B3 is believed to have a relatively high PR_{LEF} since its PR_{eq} level is lower than that for Strake B1. But no vivid oil coalescence appears (see Fig.5-16(e)) because the fin-shock impingement point is downstream of the fin-trailing edge as L_{imp}/L_{fin} indicates.

Thus it seems that the variables PR_{eq} and L_{imp}/L_{fin} reasonably correspond to the actual surface oil streak behaviour, hence the predicted geometries of the strake based on these variables are also considered to be plausible. But more experimental data at different flow conditions are desired in order to "validate" the prediction.

5.6 The effect of strake geometry on separation

Fig.5-17 illustrates several regimes from which one can see the general tendency of strake-geometry effects on separation. The hatched area in Fig.5-15 is classified as "no separation" in Fig.5-17. The geometry of Strake B3 selected from this zone at $M_\infty=2.46$ and $\alpha_{fin}=15^\circ$ (see Fig.5-15) did not have any strong oil convergence relating to significant vortical separation (see Fig.5-16(e)).

The "no separation" area is surrounded by a region where a strake can weaken separation. In this area, the effects of both the strake shock and the fin shock are diminished, such that only a weakly-separated flowfield is created. Strakes 3 and B1 may fall in this "weak" category. But as observed in Figs.5-16(b) and (c), separation is caused by the strake shock in Strake 3 whilst the fin shock has a dominant role for Strake B1.

As already mentioned, the lower edge of the fin shock tends to be weakened

when the strake shock becomes strong (see the white-arrow direction in Fig.5-17). In that case, the fin shock is no longer strong enough to cause separation even if it reaches the wall upstream of the model trailing edge. This tendency can be seen in the LFV images and the CFD flowfield of Strake 2 (see Figs.4-2(d) and 4-6(a)). Under such circumstances, line B becomes meaningless and relatively strong separation is created solely by the strake shock (see Fig.5-16(a)). The area labelled "strake-shock sep." with lighter shading in Fig.5-17 corresponds to this situation. There is no criterion for a border between the "weak separation" and the "strake-shock sep.".

On the other hand, the strength of the lower edge of the fin shock increases when the strake shock weakens (see the black-arrow direction in Fig.5-17). The CFD result for the Strake B2 (see Fig.4-6(d)) indicate this tendency. If a strake belongs to the lower right area of Fig.5-17, labelled "fin-shock sep." with darker shading, relatively strong separation is caused solely by the fin shock. The oil pattern in Fig.5-16(d) may indicate this situation. Again no clear criterion exists for the border between the "weak separation" and the "fin-shock sep.".

Under the four categories mentioned above, the fin shock is "covered" by the strake shock (see Fig.5-18(a)) and is weakened to a certain extent. But under the regime labelled "uncovered fin shock" (see the other hatched zone in Fig.5-17), the size of the strake is so small that the strake shock does not "cover" the whole fin shock (see Fig.5-18(b)). In this case, the uncovered part of the fin shock, which is equivalent to the unmodified oblique shock, causes strong separation. One can see the behaviour of this regime at the case of Strake 1 (see Figs.3-1(c) and 4-2(c)) even though this type of strake does not cover the whole chord of the fin. The boundary for this region may be calculated using the angles of both the strake shock and the fin shock but is not discussed here.

5.7 Effects of various factors on the required dimensions

Fig.5-19 displays similar charts obtained by the same procedure employed

for Fig.5-15 with different α_{fin} and M_∞ at $\delta^*/L_{fin}=0.0273$. In Fig.5-19(a), the value of α_{fin} changes from 10° to 20° with a fixed M_∞ of 2.46. On the other hand, M_∞ changes from 2.0 to 3.0 with a fixed α_{fin} of 15° in Fig.5-19(b). As can be seen in the figure, the level of PR_{eq} increases rapidly with α_{fin} and M_∞ . Lines A and B obtained in the same way as in Sec.5.4 are also marked in each figure. The hatched "no separation area" surrounded by lines A and B gradually shifts towards the right (larger L_{str}) of the figure with α_{fin} and M_∞ . The required stroke geometries read from the point R also change with α_{fin} and M_∞ .

As the values of L_{req}/L_{fin} and H_{req}/L_{fin} are read from the intersection of lines A and B (i.e. point R), they can be found mathematically. As explained in the previous section, Line B is expressed by eqs.(5-6) and (5-7). Furthermore the level of PR_{eq} is constant at the point of R and equal to the values of PR_{IPS} (shock pressure ratio needed to cause the incipient primary separation), which is a weak function of M_∞ as determined by eq.(1-2) and the oblique shock relations. Hence η at R, η_R , can be fixed by eqs.(5-4) and (5-5) in terms of PR_{IPS} . Substituting eq.(5-7) and η_R into eq.(5-6) yields

$$\frac{H_{req}}{L_{fin}} = \frac{\tan \eta_R}{\cos \alpha_{fin}} + \frac{\delta^*}{L_{fin}} \quad (5-10)$$

In order to specify the value of L_{req}/L_{fin} , line A is formulated by eq.(5-3) as

$$\frac{\pi PR_{OS} \{F(\xi) - 0.05 |M_\infty - 2.5| (\frac{\pi}{2} - \xi)\}}{2\xi} = PR_{IPS} \quad (5-11)$$

Once the value of H_{req}/L_{fin} is fixed by eq.(5-10), L_{req}/L_{fin} can be obtained by eq.(5-11) implicitly. The whole process for determining L_{req}/L_{fin} and H_{req}/L_{fin} is explained by a flow chart in Fig.5-20. From the chart, it can be seen that the values of L_{req}/L_{fin} and H_{req}/L_{fin} are affected by M_∞ , α_{fin} and δ^*/L_{fin} .

Based on Fig.5-20, specific values of L_{req}/L_{fin} and H_{req}/L_{fin} can be obtained for arbitrary M_∞ , α_{fin} and δ^*/L_{fin} . Fig.5-21(a) shows the applicable range of the prediction discussed here. From the effective range of eq.(5-3), on which eq.(5-

11) depends, the reliable range of Mach number is $2.0 \leq M_\infty \leq 5.0$. In addition to this limitation, the prediction is impossible in Region A, where α_{fin} is larger than the oblique-shock detachment limit and PR_{os} for eq.(5-11) is not obtainable. In Region B, the sweep angle (λ) of the strake specified by the prediction is larger than 70° . In this case, the accuracy of eq.(5-3) deteriorates, so that the strake geometry obtained is not reliable. In Region C, the oblique shock caused by the unmodified fin is weaker than the level for incipient primary separation (see Fig.1-8). Hence one does not have to introduce any junction modifications.

In Fig.5-21(b), the levels of L_{req}/L_{fin} and H_{req}/L_{fin} are plotted using a group of solid lines (iso- L_{req}/L_{fin} lines) and single-chain-dotted lines (iso- H_{req}/L_{fin} lines) respectively in the M_∞ - α_{fin} coordinate system for $\delta^*/L_{fin}=0.0273$. The ranges of M_∞ and α_{fin} for Fig.5-21(b) are selected from the reliable range of the prediction (see the hatched zone in Fig.5-21(a)). The present experimental condition ($M_\infty=2.46$ and $\alpha_{fin}=15^\circ$) is indicated by a solid circle in the figure. The values read from the circle, $L_{req}/L_{fin}=0.53$ and $H_{req}/L_{fin}=0.57$, equal to those obtained from the point R in Fig.5-15. From this chart, one can easily estimate the values of L_{req}/L_{fin} and H_{req}/L_{fin} for a wide range of M_∞ and α_{fin} .

For the present experimental case and most practical cases, the value of δ^*/L_{fin} is close to 0 because the size of L_{fin} is usually much larger than δ^* . Hence the effect of δ^*/L_{fin} is not discussed here. However if the value of δ^*/L_{fin} is considerable (i.e. an extreme thick boundary layer and (or) a relatively small fin), this factor must be taken into account and the effect is summarized in Appendix D.

CHAPTER 6 Conclusions

The effects of "fillet-" and "strake-" type junction modifications on the glancing interaction generated by a sharp fin were examined experimentally at $M_\infty=2.46$ and $Re=2.59 \times 10^6/m$. An unmodified fin and several modified fins were tested using oil flow visualization, surface pressure measurements, liquid crystal thermography, schlieren photography and laser light sheet flow visualization. In addition to these experimental observations, an Euler CFD solver was used to help understand the inviscid flow structures which play important roles in the interaction behaviour. Based on the data experimentally and numerically obtained, a method was proposed for predicting the geometry of the strake needed to prevent separation. The experimental and numerical results and the predictions based on the results lead to the following conclusions.

- 1) A "strake-type" junction modification has a weakening effect on separation.
- 2) The fillet tested in this study strengthened separation rather than weakened it.
- 3) When a sharp fin is modified using a strake, a shock wave from the strake leading edge (strake shock) intersects the oblique shock from the unmodified part of the fin (fin shock) and bends it strongly toward the fin. The bent fin shock approaches the wall quite linearly with distance from the leading edge. The attachment/detachment of the strake shock from the strake leading edge does not affect the linearity of the fin shock approaching. The strengths of the strake shock and the fin shock have strong mutual dependence.
- 4) Separation can not be prevented or weakened by junction modification

techniques unless the shock system is weakened.

- 5) In order to prevent separation, a strake must be tall enough to prevent the fin shock from reaching the wall until the trailing edge. In addition the strake must have a small apex angle and a large sweep angle so that the strake shock itself does not cause separation. Based on these conditions, the strake geometries needed to prevent the interaction-induced separation can now be predicted. This prediction is applicable for a wide range of Mach number, boundary-layer displacement thickness and the unmodified-fin wedge angle.
- 6) In the process of predicting the necessary strake geometries, generalized relations for the shock strength and shock angle caused by rhombic delta wings were constructed. They not only provide the proper values of the shock strength and angle for predicting the required strake geometry, but also help in understanding the flowfields around rhombic delta wings.

Acknowledgements

The author would like to express the deepest gratitude to Professor J. L. Stollery for his expert guidance, invaluable advice and encouragement during the course of this study. Thanks also goes to the Technical Research & Development Institute of the Japan Defense Agency for the financial support by which this study has been carried out.

The computational data were provided by Mr. C.W.J. Griesel as a part of collaborative study with the author in order to develop a valid CFD solver. Useful discussions with him are too many to mention.

The laser for the LFV visualization was supplied by Professor P. Hutchinson, Professor R.L. Elder and Dr. J. Edwards. Liquid crystals were also provided by Dr. J. Edwards. Mr. H. Babinsky gave the author a lot of precious advices in order to develop the liquid crystal thermography technique. My sincere thanks to them all.

The author is also greatly indebted to all staff in the workshop and wind tunnel section not only for model manufacturing and tunnel operation but also for their practical advices and encouragements.

Special thanks are in order for my patient long suffering wife, Maki, and children, Yayoi & Takuya who have always supported me mentally and physically.

Last but not the least, particular appreciation is to Mr. S. Blank for making my stay in U.K. very comfortable.

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APPENDIX A A design of the laser light sheet generator

In order to construct an optical system as the laser light sheet generator (see Fig.2-15), sequential geometrical-optics calculations were carried out. This appendix briefly outlines the way in which the optics were arranged for the present laser light sheet generator. Ref.A1 provides the details of the procedure of geometrical optics, some of which have been paraphrased in this appendix.

Before going to the calculation, a basic combination of the optics must be first considered. In order to vary the height and the focal-point location of the laser sheet in accordance with the configurations of several different models, a combination of two spherical lenses and two cylindrical lenses was employed (see Fig.A-1. This concept was derived from Ref.29). The laser beam is first focused in the centre of the interesting section using the two spherical lenses labelled S1 and S2. The light path shown by dotted line in Fig.A-1 indicates the beam created by S1 and S2. The output of S2 is then expanded in one axis using a combination of two cylindrical lenses C1 and C2. As a result a thin light sheet, whose top view and side view are drawn by the dotted line and solid line respectively, is created. The two spherical lenses allow the focus of the laser beam to be changed by varying the spacing t_{12} . Similarly the sheet height, h , can be controlled by adjusting the spacing t_{56} .

Once the basic combination of the lenses was established, the location and configuration of each lens were considered. The principle of the calculations is a very simple law known as "Snell's Law" about the refracted light. When the light passes the boundary of the two different mediums (see Fig.A-2), refracted light passes through the boundary by obeying Snell's Law;

$$n \sin\theta = n' \sin\theta' \quad (A-1)$$

where n and n' are the "refractive index" constants of the two mediums. θ and θ' are the angles the ray of light marks to the normal of the surface at the point of incidence before and after refracting, respectively (see Fig.A-2). As long as the

values of θ and θ' are small, eq.(A-1) can be approximated as

$$n\theta = n'\theta' \quad . \quad (A-2)$$

In order to consider a light path through multiple lenses, YNU RAY TRACING method, developed by the Institute of Optics at the University of Rochester, was employed. This method is a simple yet quite accurate for tracing the path of light rays through an optical system. When a paraxial ray of height y with an angle of inclination to the optical axis of u strikes a spherical surface of radius r_c (see Fig.A-3), an angle of incidence, θ , can be written as

$$\theta = \delta + u = \frac{y}{r_c} + u \quad . \quad (A-3)$$

Substituting this into eq.(A-2) yielding

$$n'u' = nu - y\phi \quad (A-4)$$

where ϕ is

$$\phi = \frac{n' - n}{r_c} \quad . \quad (A-5)$$

After refracting the ray travels a distance t within the material, it will encounter the next surface at a height predicted by

$$y_2 = y_1 + tu \quad (A-6)$$

whereupon it will again refract. Sequentially applying these formula at and between each surface of a given optical system for a specified entering ray yields an accurate prediction of the path that ray will follow.

A YNU Ray Trace Worksheet developed at the Univ. of Rochester (see Table A-1) can further simplify this procedure. Use of this chart involves;

- (1) Entering the lens system data in Part A.
- (2) Calculating the quantities in Part B.

- (3) Entering a initial ray height (y_0) and $n_{01}u_{01}$ in Part C. $n_{01}u_{01}$ is calculated by eqs.(A-4) and (A-5) as

$$n_{01}u_{01} = n_0u_0 - y_0 \frac{n_{01} - n_0}{r_0} \quad (\text{A-7})$$

where n_0 and u_0 are refracted index and ray angle before entering the lens system respectively.

The ray can then be translated or refracted by;

- (4) Multiplying it by the number directly above it in Part B;
- (5) Adding to the product obtained the value above or below it and one-half space to the left in Part C; and,
- (6) Recording this (the desired) value one-half space to the right in Part C.

Table A-1 shows two concrete examples how to progress the calculation, i.e.

$$y_1 = y_0 + n_{01}u_{01} \times \frac{t_{01}}{n_{01}}$$

$$n_{34}u_{34} = n_{23}u_{23} + y_3 \times (-\phi_3) \quad .$$

This process is repeated sequentially until the entire system has been transversed. The preconditions for the calculations were;

- (1) The diameter of the laser beam was about 2mm from the specification of INNOVA 70-4. Therefore, y_0 was set to 1mm.
- (2) The laser beam travels parallel to the optical axis. Therefore, u_0 was set to 0° . Hence the value of $n_{01}u_{01}$ calculated by eq.(A-7) is -0.044 (where n_0 was taken as 1.0).
- (3) From the geometrical relations among the model, the tunnel window and the light sheet generator (see Fig.A-4), t_{78} must be more than 222mm

($\geq 173.7 + 48.0$).

- (4) The distance between the points of entry and departure of a light beam passing through a lens, labelled t in Fig.A-5, was almost same as the lens thickness, t_{lens} , in the case of spherical lens, but the difference between t and t_{lens} could not be ignored for the cylindrical lens (see Fig.A-5). Therefore, t_{01} and t_{23} were taken as the same value of each lens-thickness but t_{45} and t_{67} were first set to each lens-thickness temporarily and were then modified based on the results of each trial-calculation.
- (5) In order to cover the whole interaction flow field, $y_8 (=h/2)$ must be more than 40mm.

Tables A-1 and A-2 show the final results of lens' configurations and their locations.

Reference

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APPENDIX B A brief outline of Griesel's CFD solver

In Griesel's CFD solver, a Riemann problem based shock capturing method was applied to the solution of the steady Euler equations. The basic concept of the numerical formulation used in his scheme is outlined in this appendix, details of which are available in Ref.79.

B.1 Governing equations

For steady-state inviscid flows of a perfect gas, the Euler equations in Cartesian coordinates for three-dimensional flow can be written by a combination of conservation equations of mass and momentum and energy equation as

$$\frac{\partial F}{\partial x} = -\frac{\partial G}{\partial y} - \frac{\partial H}{\partial z} \quad (\text{B-1})$$

where the vectors F, G and H are

$$F = \begin{pmatrix} \rho u \\ \rho u^2 + P \\ \rho uv \\ \rho uw \\ u(E+P) \end{pmatrix} \quad G = \begin{pmatrix} \rho v \\ \rho vu \\ \rho v^2 + P \\ \rho vw \\ v(E+P) \end{pmatrix} \quad H = \begin{pmatrix} \rho w \\ \rho wu \\ \rho wv \\ \rho w^2 + P \\ w(E+P) \end{pmatrix} .$$

Here P and ρ are the static pressure and density of the gas and u, v and w are the x, y and z components of velocity respectively. The total energy per unit volume, E, consisting of the kinetic and internal energies is given by

$$E = \frac{\rho (u^2 + v^2 + w^2)}{2} + \frac{P}{\gamma - 1}$$

with γ denoting the ratio of specific heats.

When the right-hand-side of eq.(B-1) is expressed in terms of finite difference in y and z directions, the problem reduces to solving a set of ordinary differential equations of F with respect to x using appropriate boundary and initial conditions (known as **Space Marching Method**). In the marching process, shock waves are captured naturally as a sharp change of the fluid state (**Shock Capturing**

Method).

B.2 Numerical integration

If a control volume illustrated in Fig.B-1 is introduced for a computational cell which has a relation with neighbours as explained in Fig.B-2, eq.(B-1) can be expressed as

$$F_i^{n+1} = F_i^n - (G_{j+\frac{1}{2}}^n - G_{j-\frac{1}{2}}^n) \frac{dx}{dy} - (H_{k+\frac{1}{2}}^n - H_{k-\frac{1}{2}}^n) \frac{dx}{dz} \quad (B-2)$$

where superscript n corresponds to a space step shown in Fig.B-2(b). Once the vector F_i^{n+1} is calculated through the integration, one can define a quadratic equation for the density ρ by eliminating all the other variables.

$$A\rho^2 + B\rho + C = 0 \quad (B-3)$$

where

$$A = \frac{F_3^2}{F_1^2} + \frac{F_4^2}{F_1^2} - 2 \frac{F_5}{F_1}$$

$$B = \frac{2F_2\gamma}{\gamma-1} \quad C = -F_1^2 \frac{\gamma+1}{\gamma-1} .$$

and F_1 through F_5 are the vector components of F_i^{n+1} . The physical meaningful root of eq.(B-3) gives

$$\rho = \frac{-B + \sqrt{B^2 - 4AC}}{2A} .$$

All the other state variables are then given by

$$P = F_2 - \frac{F_1^2}{\rho} \quad u = \frac{F_1}{\rho} \quad v = \frac{F_3}{F_1}$$

$$w = \frac{F_4}{F_1} \quad E = \rho \frac{F_5}{F_1} - P \quad .$$

The state variables of the flow are considered as piecewise constant over each computational cell.

In order to complete the formula of eq.(B-2), the intercell fluxes of G and H must be calculated at each space step. To find them, Griesel's scheme employed what is called "Riemann solver". In this solver, the evolution of the flow to the next space step results from the wave interactions originating at the boundaries between adjacent cells (the **Riemann problem**). The Riemann problem has an exact solution generally composed of three waves separating regions of uniform flow conditions. The first and third waves could be oblique shock or expansion fan, between which the slip line is placed as the second wave. The solutions to the Riemann problem at each cell interface may produce a perturbation of the piecewise constant fluid state, resulting from the waves over the space interval dx . Based on the relations of pressure and flow direction with those of the adjacent cell, intercell fluid state variables can be calculated in terms of the relations of the oblique shock and (or) the Prandtle-Meyer expansion. Once the variables are fixed at several intercell points, the G and H fluxes are obtained by averaging, over each cell, the fluid states. In Griesel's scheme, this averaging was carried out by the Weighted Average Flux (WAF) method first formulated by Toro^(B1), details of which are obtained also in Ref.79. In terms of this method, the G and H fluxes can be generated as a area-weighted average of the flux across the entire wave structure obtained from the solution to the Riemann problem.

In the space marching process, the space step size, dx , must be carefully chosen to ensure the numerical stability such that the waves issued at an interface of the cell do not interact with the waves created at the adjacent interfaces.

B.3 Initial and boundary conditions

As the initial condition for the integration to start with, all the state variables

were set to the corresponding free stream variables at the first y-z plane labelled I in Fig.B-3.

On the computational boundary, "transmissive" and "reflective" boundary conditions were introduced for non-solid and solid boundaries respectively. For the transmissive boundary condition applied for the boundaries labelled A through D in Fig.B-3, imaginary ghost cells, whose properties are equal to their counterparts in the computational domain, are constructed outside of the boundary.

At the reflective boundary employed for the solid surface labelled S in Fig.B-3, the variables of P , ρ and M are treated in the same way as the transmissive boundary, but the velocity is adjusted such that the slip line of the Riemann problem coincides with the tangential direction of the body. As a result, there is no velocity component normal to the body surface (slip condition). In the present case, the reflective boundary was also applied for the plane of symmetry labelled P in Fig.B-3 because the flow is parallel to them.

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APPENDIX C A correlation law of the strake-shock angle with the strake geometry and M_∞

A correlation law of β_{str} with the strake geometry and M_∞ was constructed using a similar way to the equivalent pressure ratio, PR_{eq} . This correlation law was then employed to predict the strake-shock location of Strake B3 (see Sec. 5.5 and Fig.5-16(e)). The solid circles in Fig.C-1 show the values of β_{str} obtained by the rhombic-delta wing (RDW) flowfield calculation at $M_\infty=2.0, 2.46$ and 3.5 .

A simple correlation using ξ (see Fig.5-10) and β_{os} (the two-dimensional oblique-shock angle with the same α_{str} , see the values at $\lambda=0^\circ$ in Fig.C-1) did not collapse the data sufficiently as shown in Fig.C-2(a). After a number of trials, the data were plotted by

$$\frac{\beta_{str}}{\beta_{os}} = \frac{2\xi}{\pi M_\infty^2} \quad (C-1)$$

where

$$a = \frac{\frac{\pi}{2} - \xi}{3} \quad (C-2)$$

and ξ is expressed in radians. This plot greatly improved the data correlation with ξ (see Fig.C-2(b)). The RDW shock angles calculated by Deng et al.^(C1) (see the stars in Fig.C-2(b)) are also quite close to the correlation line. The value of β_{str} is then determined directly from

$$\beta_{str} = \frac{\pi \beta_{os} F(\xi) M_\infty^2}{2\xi} \quad (C-3)$$

where $F(\xi)$ was obtained by the least squares method as

$$F(\xi) = A_0 + A_1\xi + A_2\xi^2 + A_3\xi^3 + A_4\xi^4$$

$$\begin{pmatrix} A_0 \\ A_1 \\ A_2 \\ A_3 \\ A_4 \end{pmatrix} = \begin{pmatrix} -0.2504 \\ 0.8081 \\ -0.1829 \\ -0.0971 \\ 0.1318 \end{pmatrix} .$$

The values of β_{str} obtained by the CFD calculations and eq.(C-3) are compared in Fig.C-1 at various M_∞ , α_{str} and λ . The agreement is very good. The strake shock angles read from Fig.4-6 also coincide with the corresponding angles predicted by eq.(C-3).

The same extreme cases applied for PR_{eq} were employed again in order to estimate the effective range of eq.(C-3) (see the cases marked by a solid circle, a solid triangle, a solid box and a cross in Fig.C-2(b)). Based on these data, eq.(C-3) has been estimated to be quite accurate (within 4% of the corresponding CFD value) for the range $2.0 \leq M_\infty \leq 5.0$, $0^\circ \leq \alpha_{str} \leq 20^\circ$ and $0^\circ \leq \lambda \leq 70^\circ$. In terms of eq.(C-3), β_{str} can be easily specified for arbitrary α_{str} , λ and M_∞ within the effective range of eq.(C-3).

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- C1 DENG, X. et al., "Improvement of Conical Similarity Rules in Swept Shock Wave/Boundary Layer Interaction", AIAA 93-2941.

APPENDIX D The effect of δ^*/L_{fin} on the required strake geometry

The ratio of δ^* to L_{fin} is one of the factors which affect the values of L_{req} and H_{req} . As mentioned in Sec.5.7, this factor must be taken into account when the value of δ^*/L_{fin} is considerable (i.e. an extreme thick boundary layer and (or) a relatively small fin). The position of line B for several values of δ^*/L_{fin} at $M_\infty=2.46$ and $\alpha_{fin}=15^\circ$ is shown in Fig.D-1, with line A marked by a single-chain-dotted line. The inviscid strake shock strength is introduced independently from the viscous layer and as far as a turbulent boundary layer is concerned, Korkegi's criterion (on which line A is based) is free from the viscous effects, because it ignores the effect of Re related to characteristics of the boundary layer as discussed in Sec.1.1.1. Hence at fixed α_{fin} and M_∞ , the position of line A is the same, regardless of the boundary layer properties. But when the value of δ^*/L_{fin} changes at fixed M_∞ and α_{fin} , the position of line B varies linearly with δ^*/L_{fin} as shown in eq.(5-6). From the intersections between line A and the group of line B, one can find the trends of L_{req}/L_{fin} and H_{req}/L_{fin} against the value of δ^*/L_{fin} . When the value of δ^*/L_{fin} increases, the values of both L_{req}/L_{fin} and H_{req}/L_{fin} increase, as observed in Fig.D-1.

Based on the flow chart in Fig.5-20, concrete values of L_{req}/L_{fin} and H_{req}/L_{fin} can be obtained for arbitrary δ^*/L_{fin} . They are arranged by α_{fin} - δ^*/L_{fin} coordinate at $M_\infty=2.46$ (see Fig.D-2) and M_∞ - δ^*/L_{fin} coordinate at $\alpha_{fin}=15^\circ$ (see Fig.D-3). In each figure, the levels of L_{req}/L_{fin} and H_{req}/L_{fin} are expressed by a group of solid lines (iso- L_{req}/L_{fin} lines) and single-chain-dotted lines (iso- H_{req}/L_{fin} lines) respectively. The present experimental condition ($M_\infty=2.46$, $\alpha_{fin}=15^\circ$ and $\delta^*/L_{fin}=0.0273$) is marked by a solid circle at each figure. The values read from the circle, $L_{req}/L_{fin}=0.53$ and $H_{req}/L_{fin}=0.57$, equal to those obtained from the point of R in Fig.5-15. In terms of these chart, one can easily estimate the effect of δ^*/L_{fin} on the values of L_{req}/L_{fin} and H_{req}/L_{fin} at arbitrary M_∞ and α_{fin} .

Table 1-1 Test conditions for experiments in the supersonic regime

AUTHOR	M_∞	Re/m	δ (mm)	METH. *	Ref.
Lakshmanan	2.40	2.6×10^6	50	num.	61
Haq	6.85	3.2×10^7		exp.	25
Blank	2.45	2.6×10^6	15.7	exp.	62,63
Kleifges	4.95	4.9×10^7	15.0	exp.	64

* The methods of the investigations,
exp. = experimentally
num. = numerically

Table 1-2 Test conditions for experiments in the low subsonic regime

AUTHOR	U(m/s)	Re	δ (mm)	METH. * ¹	Ref.
Kubendran	26.5	1.7×10^6 /m		exp.	65
Sung	*2	6.2×10^5 /C* ³	$0.06C^*3$	num.	66
Devenport	27.0	6.6×10^3 /θ	35.9	exp.	67
	32.5	4.5×10^3 /θ	17.9		68

*1 Same as Table 1-1

*2 Velocities normalized by a freestream velocity were employed.
Governing equation was an incompressible Navier-Stokes eq..

*3 Chord length of the wing

Table 2-1 Test matrix

Tunnel	Model		OF	PM	SP	LFV	LC	CFD
9"×9"	Group A	Unm	✓	✓	✓	✓		
		Fillet	✓	✓	✓	✓		
		Str1	✓	✓	✓	✓		
		Str2	✓	✓	✓	✓		✓
		Str3	✓	✓	✓	✓		✓
	Group B	StrB1	✓					✓
		StrB2	✓					✓
		StrB3	✓					
2.5"×2.5"	Group C	UnmC					✓	
		Str2C					✓	
		StrB2C					✓	
		RDW			✓			✓

Abbreviations

- Unm : Unmodified fin
- Str : Strake
- RDW : Rhombic delta wing
- OF : Oil flow visualization
- PM : Surface pressure measurement
- SP : Schlieren photography
- LFV : Laser light sheet flow visualization
- LC : Liquid crystal thermography

Table 2-2 Liquid crystal colour changing temperature

Colour	Temperature (°C)
Start of changing	27.6
Red (Brown)	28.4
Blue green	29.6
Light blue	34.1
Colourless	41.5

Table 4-1 Detachment similarity parameter for each strake

STRAKE	M_{nle}	α_{nle} (deg)	ζ	attach/detach
2	2.22	13.6	35.1	attach
3	2.00	13.8	37.3	attach
B1	1.65	14.2	42.4	atta.(deta.?)
B2	1.06	21.5	67.2	detach
B3	1.68	12.7	40.7	attach

Table 5-1 Fin-shock propagation angles read from the CFD results and predicted by eq.(5-5)

STRAKE	M_∞	η (CFD)	η (eq.(5-5))	difference
2	2.46	29.5	29.3	0.2
3	2.46	29.0	28.3	0.7
B1	2.46	28.5	27.0	1.5
B2	2.46	25.5	26.1	0.6
2	2.06	36.3	36.1	0.2
3	2.06	35.0	34.6	0.4

Table A-1 YNU ray trace worksheet and the calculation result

PART A	SURFACE RADIUS	r_0	r_1	r_2	r_3	r_4	r_5	r_6	r_7	
		11.86	-11.86	64.22	-64.22	∞	-9.82	∞	-52.03	
	THICKNESS OR SEPARATION	t_{01}	t_{12}	t_{23}	t_{34}	t_{45}	t_{56}	t_{67}	t_{78}	
PART B		6.68	82.60	5.54	60.00	4.00	65.00	6.08	264.92	
	REFRACTIVE INDEX	n_{01}	n_{12}	n_{23}	n_{34}	n_{45}	n_{56}	n_{67}	n_{78}	
		1.523	1.000	1.523	1.000	1.517	1.000	1.523	1.000	
PART C	$-\phi = (n-n')/r$		$-\phi_1$	$-\phi_2$	$-\phi_3$	$-\phi_4$	$-\phi_5$	$-\phi_6$	$-\phi_7$	
			-0.0441	-0.0081	-0.0081	0.0	-0.0526	0.0	-0.0101	
	t/n	t_{01}/n_{01}	t_{12}/n_{12}	t_{23}/n_{23}	t_{34}/n_{34}	t_{45}/n_{45}	t_{56}/n_{56}	t_{67}/n_{67}	t_{78}/n_{78}	
PART C		4.39	82.60	3.64	60.00	2.64	65.00	3.99	264.92	
	y	y_0	y_1	y_2	y_3	y_4	y_5	y_6	y_7	y_8
		1.00	0.80	-5.77	-5.89	-4.97	-4.93	12.94	14.04	49.50
PART C	nu	$n_{01}u_{01}$	$n_{12}u_{12}$	$n_{23}u_{23}$	$n_{34}u_{34}$	$n_{45}u_{45}$	$n_{56}u_{56}$	$n_{67}u_{67}$	$n_{78}u_{78}$	
		-0.044	-0.080	0.033	0.015	0.015	0.275	0.275	0.134	

*1 Surface curvatures are positive if the centre of curvature lies to the right of the surface, negative if it lies to the left.

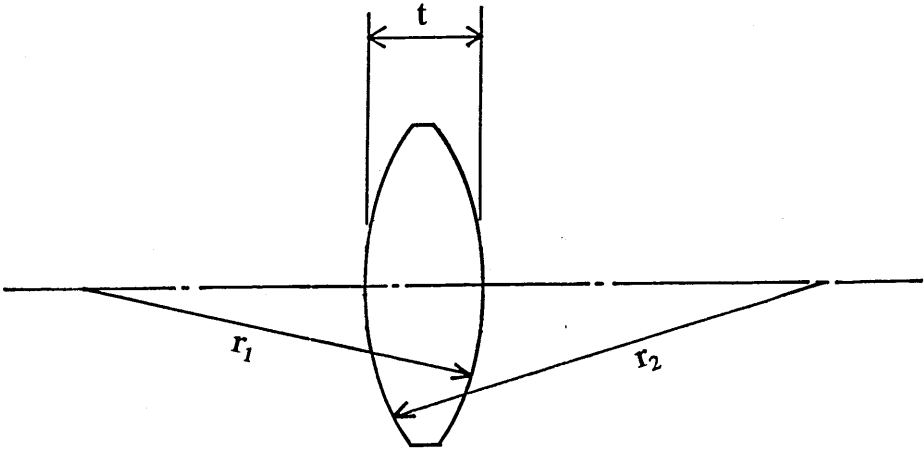
*2 The curvature of the flat surface of the cylindrical lens is infinity. Hence the value of 1.0×10^8 is introduced for the calculation.

*3 Negative y indicates the position below z axis (see Fig.A-1)

Table A-2 Details of the lenses

LENS	MAKE	MODEL	r_1	r_2	t	n
S1	Newport	KBX022	11.86	11.86	6.68	1.523
S2	Newport	KBX055	64.22	64.22	5.54	1.523
C1	Newport	CKX019	∞	9.82	5.33	1.517
C2	Ealing	23-7416	∞	52.03	8.00	1.523

n : Refracted index



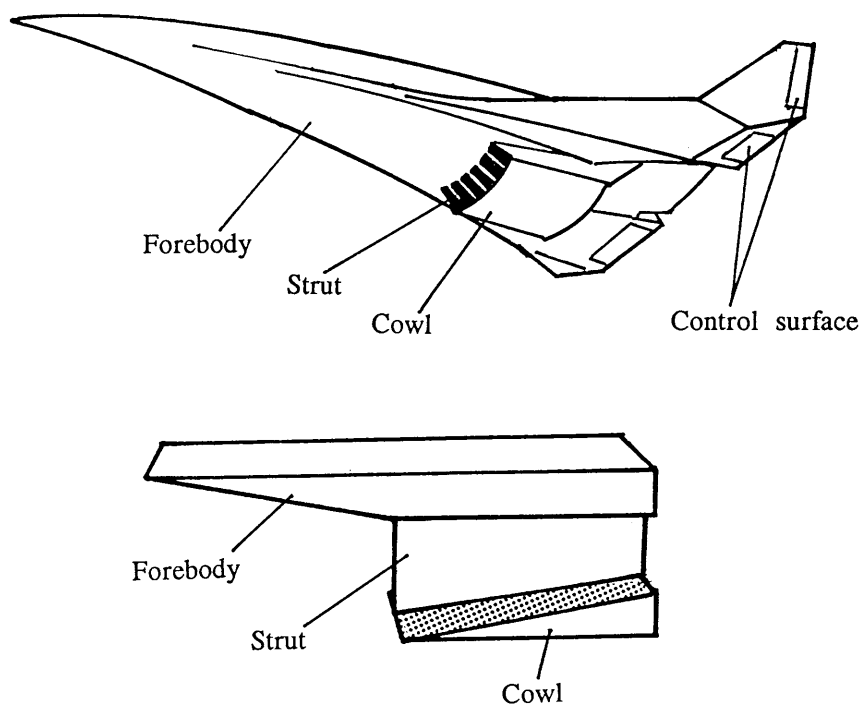


Fig.1-1 A high-speed flight vehicle and its air intake model
(reproduced from Ref.1)

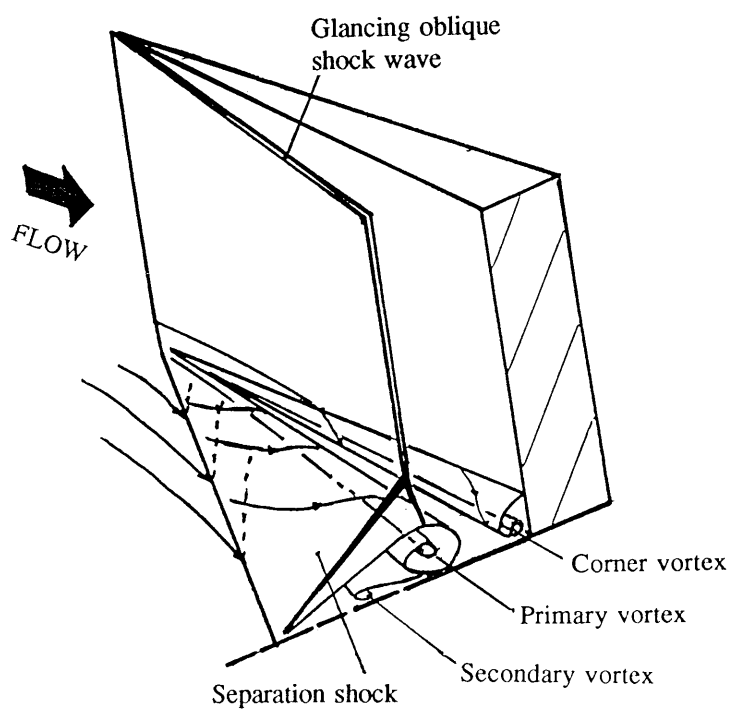
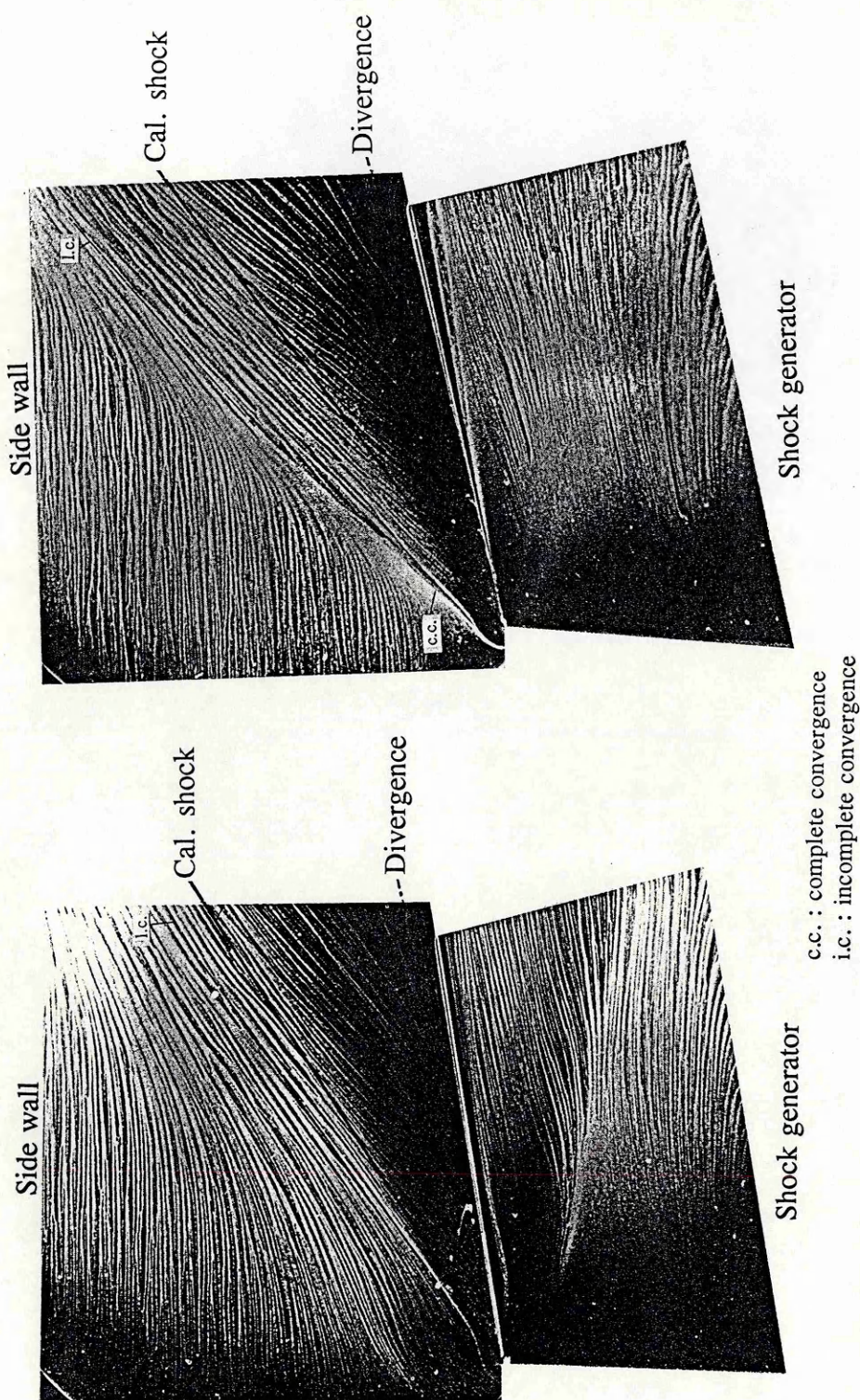
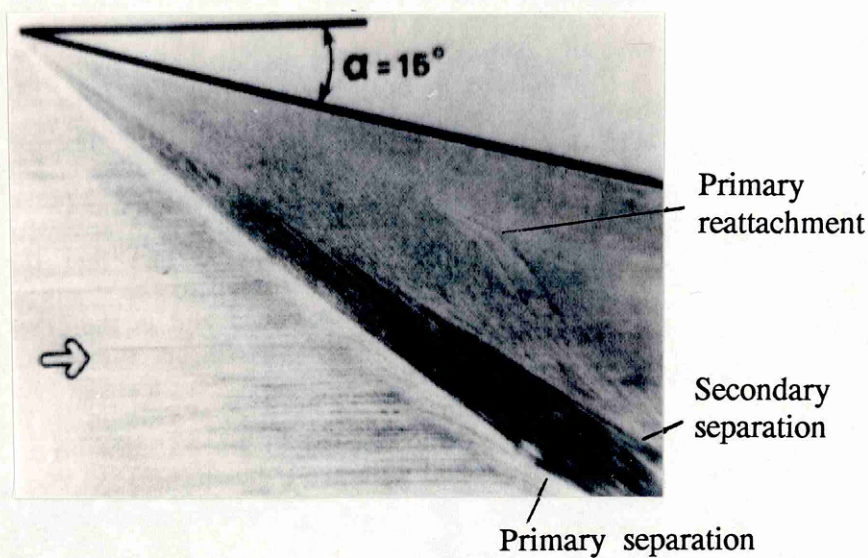


Fig.1-2 Sharp-fin-induced glancing shock wave/boundary layer
interaction



(a) A condition close to the incipient primary separation ($M_\infty=2.3$, $\alpha=9^\circ$. Ref.12) (b) Primary separation with "complete" oil convergence ($M_\infty=2.3$, $\alpha=11^\circ$. Ref.12)

Fig.1-3 Surface streak patterns of sharp-fin induced interactions



(c) Appearance of secondary separation
($M_\infty = 2.96$, $\alpha = 15^\circ$. Ref.13)

Fig.1-3 Continued

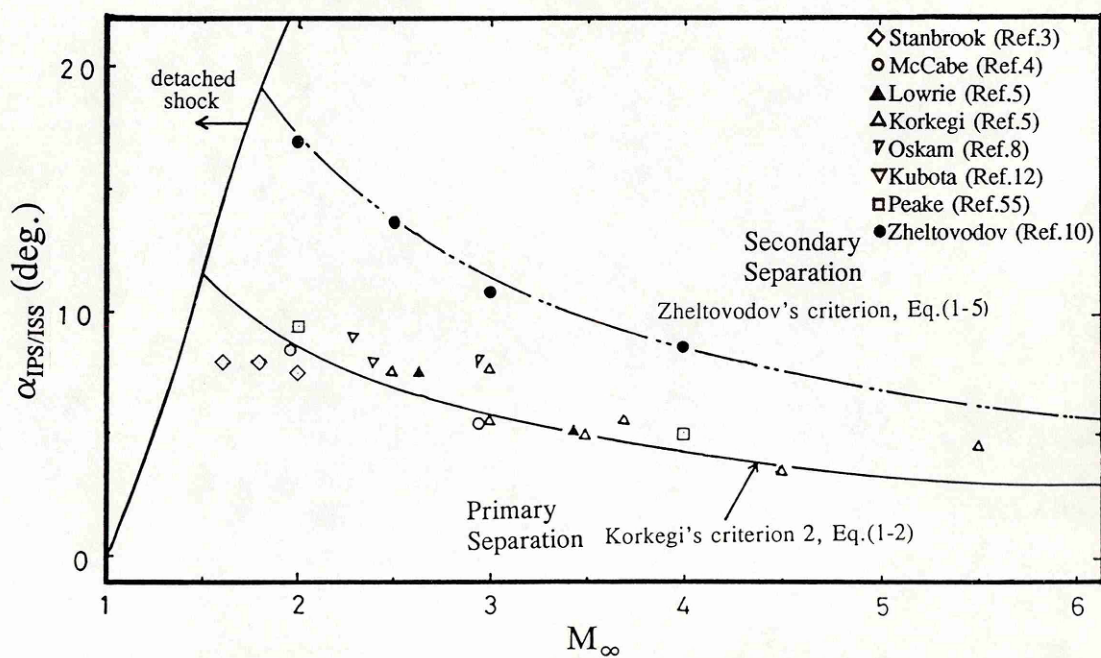
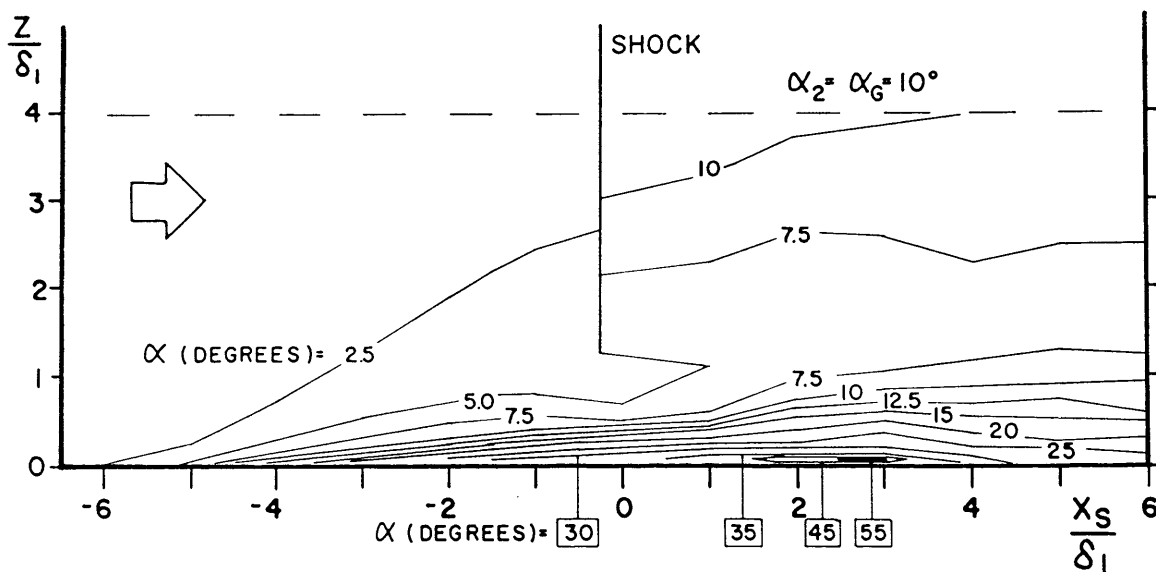
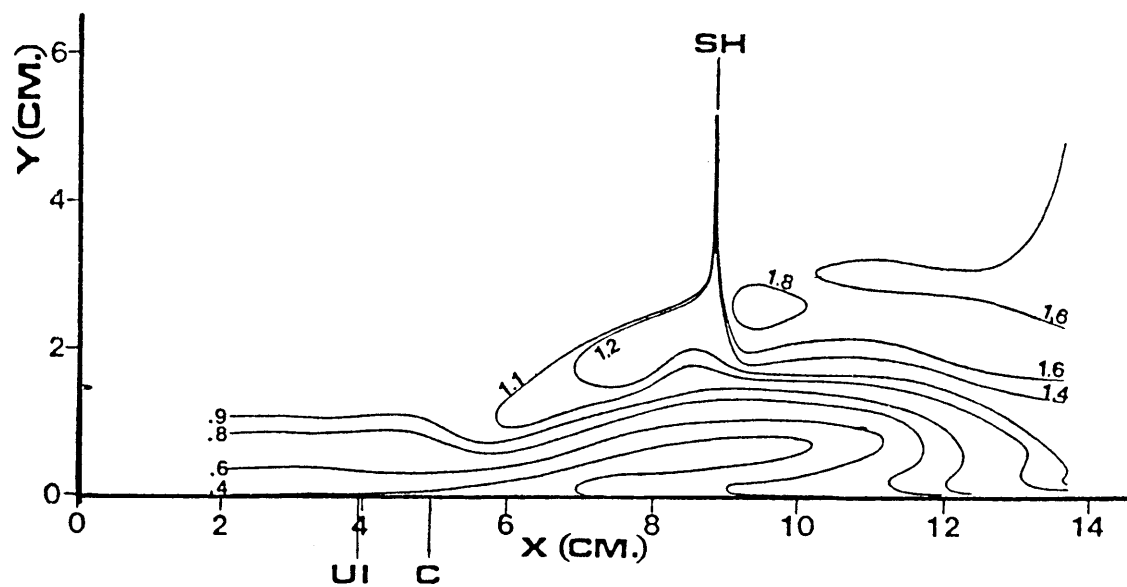


Fig.1-4 Shock generator angles to induce incipient primary- and secondary separation



(a) Yaw angle contour plot ($M_\infty=2.95$, $\alpha=10^\circ$. Ref.8)



(b) Normalized total head contours ($M_\infty=2.95$, $\alpha=20^\circ$. Ref.45)

Fig.1-5 Flowfield structures observed by intrusive probes

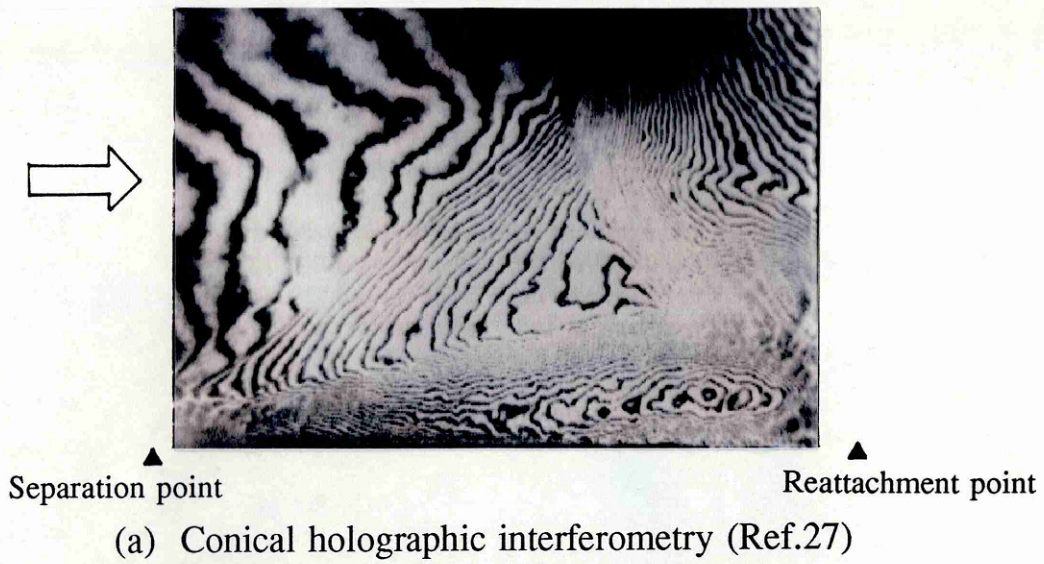
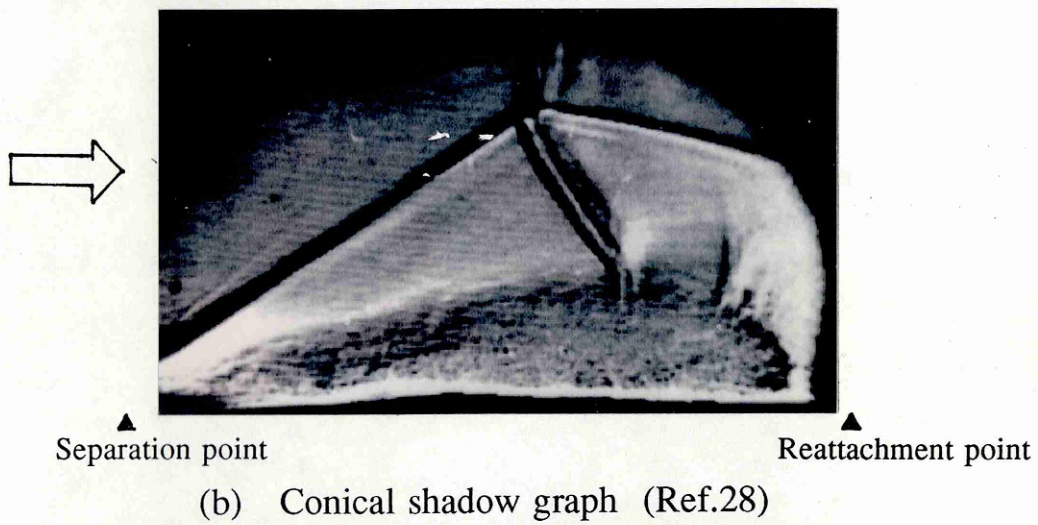
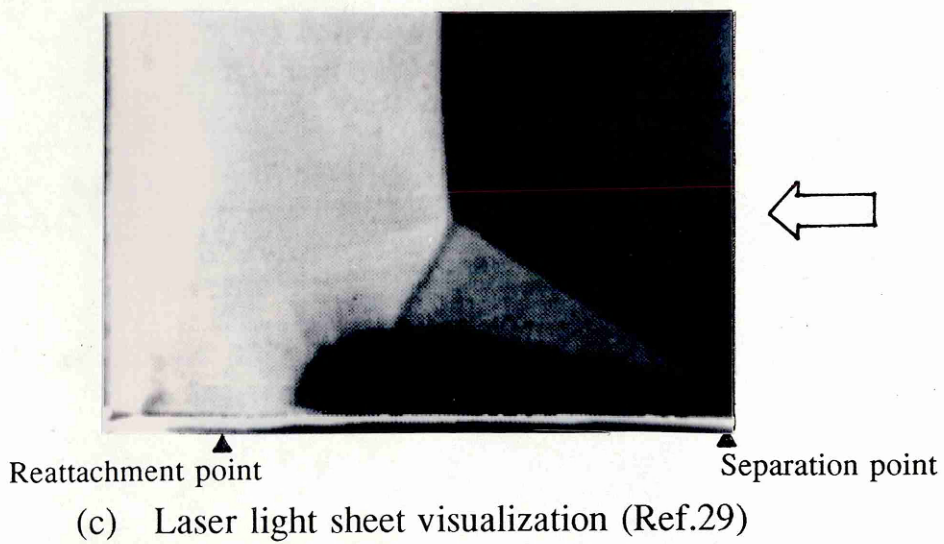


Fig.1-6 Flowfield images obtained by several optical methods

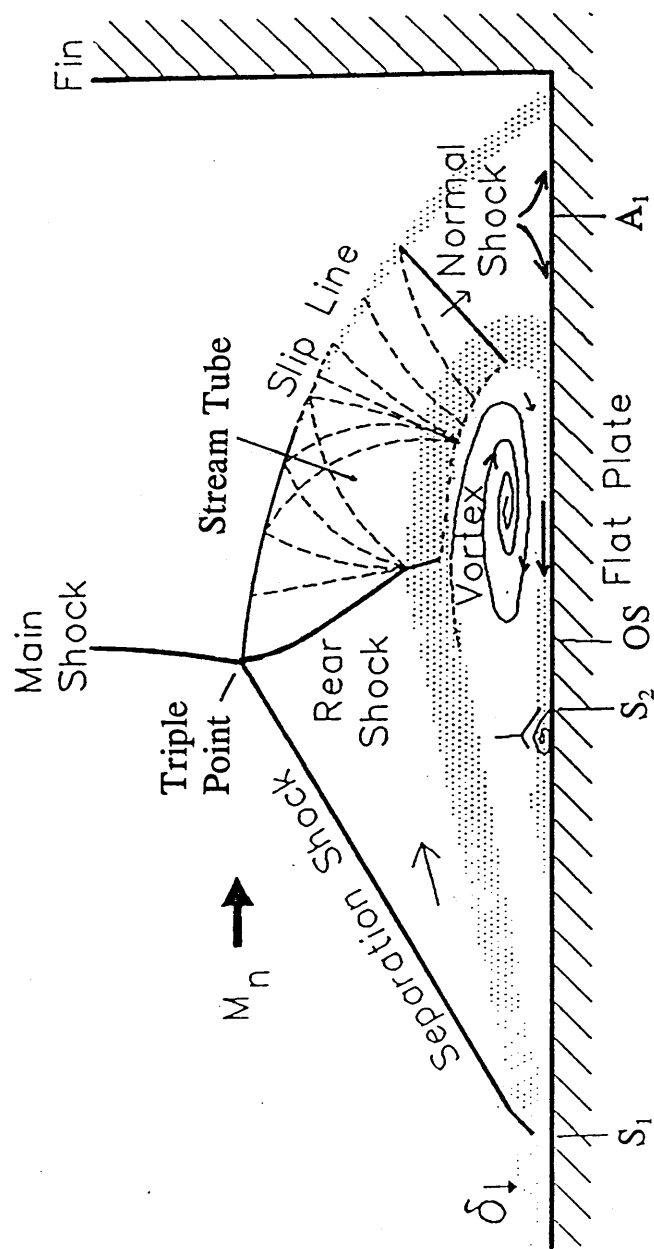


Fig.1-7 Schematic view of the flowfield structure (Ref.28)

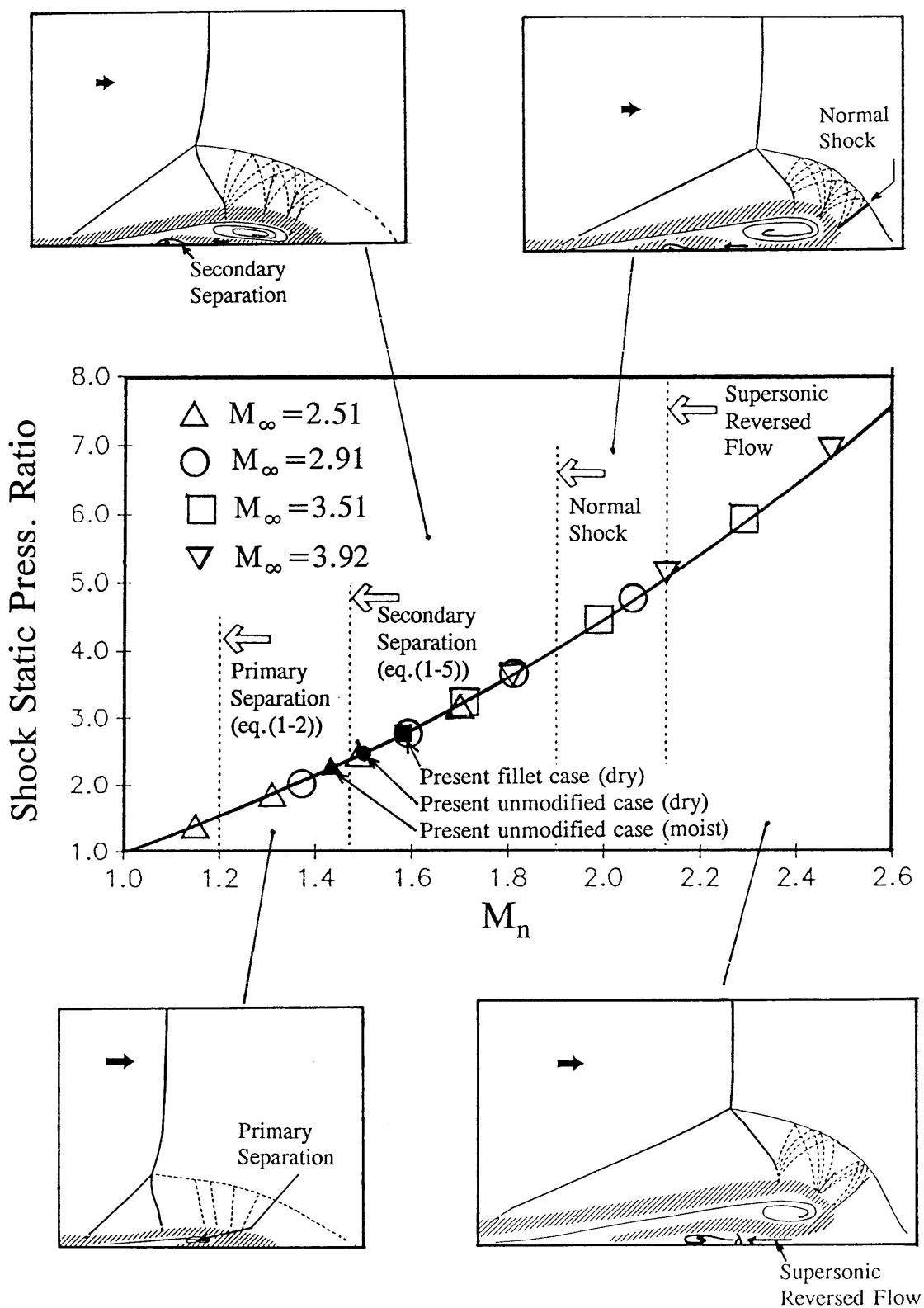


Fig.1-8 Interaction flow regimes classified by a Mach number component normal to the shock wave and shock pressure ratio (Ref.28 & 29)

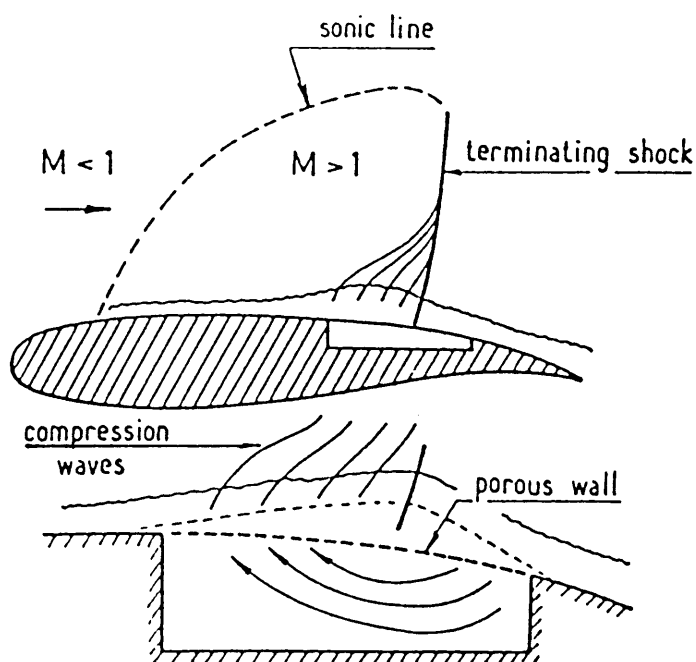


Fig.1-9 Passive shock wave/boundary layer control applied to a transonic airfoil (Ref.47)

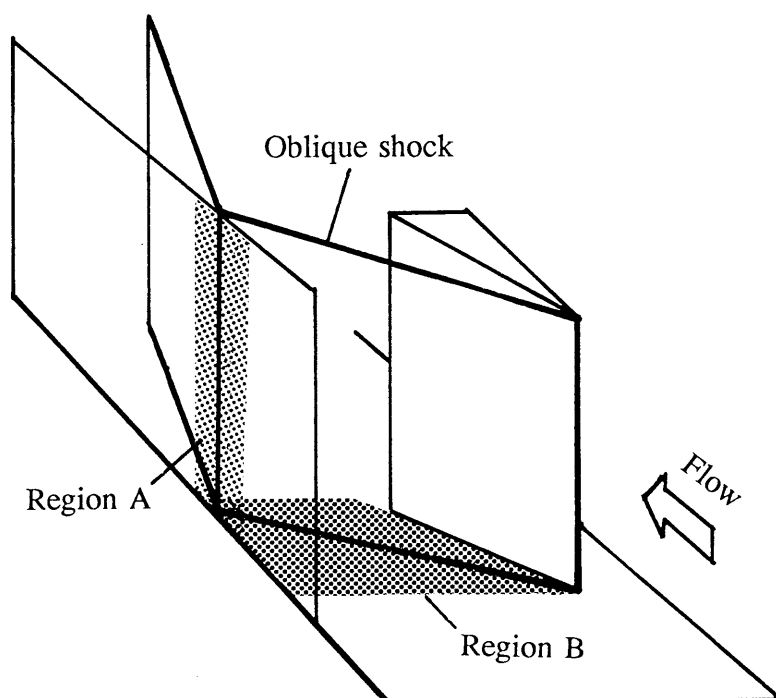
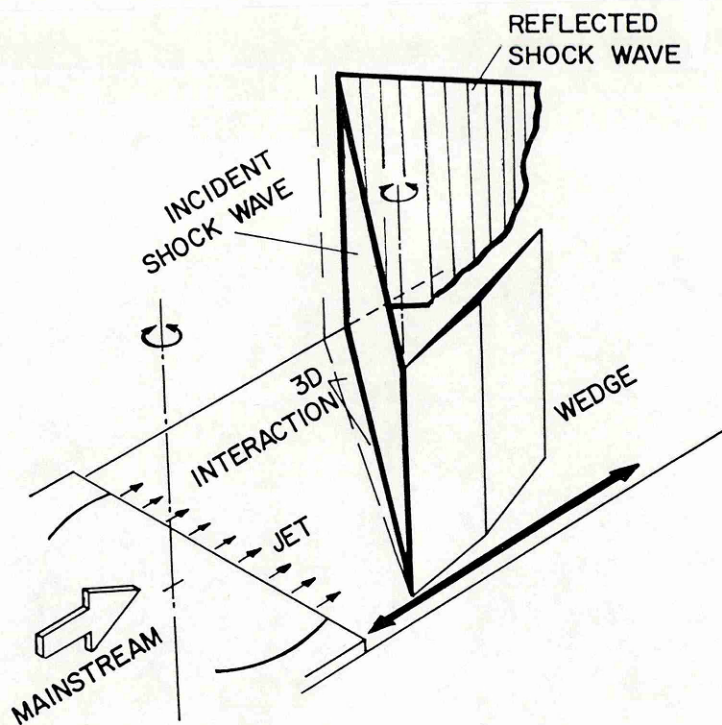
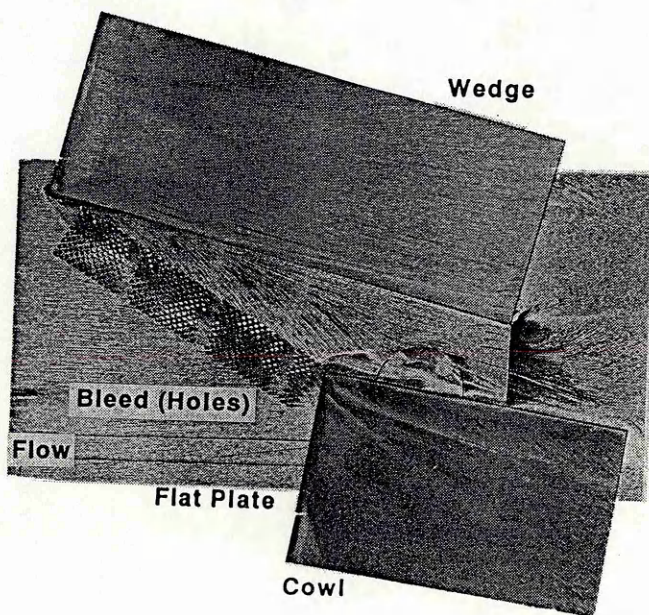


Fig.1-10 Two types of interaction caused by a sharp fin placed in a supersonic duct flow

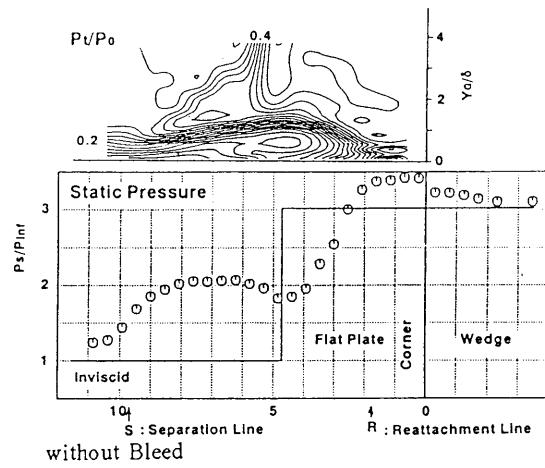
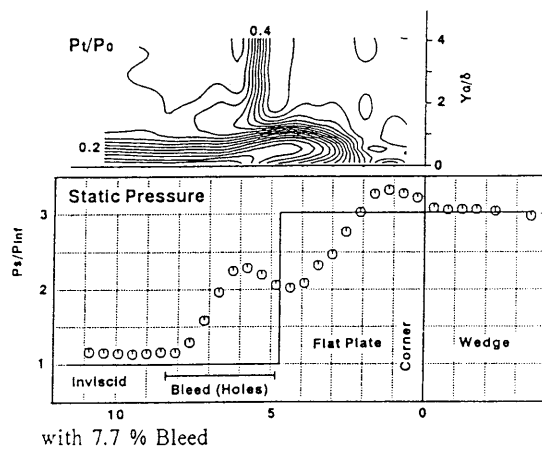
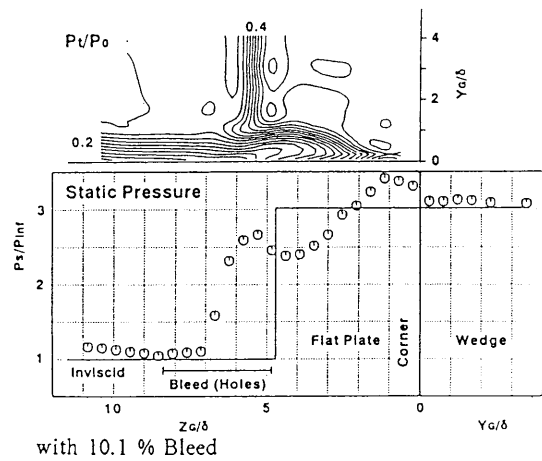


(a) Peak's tangential injection model (Ref.55)



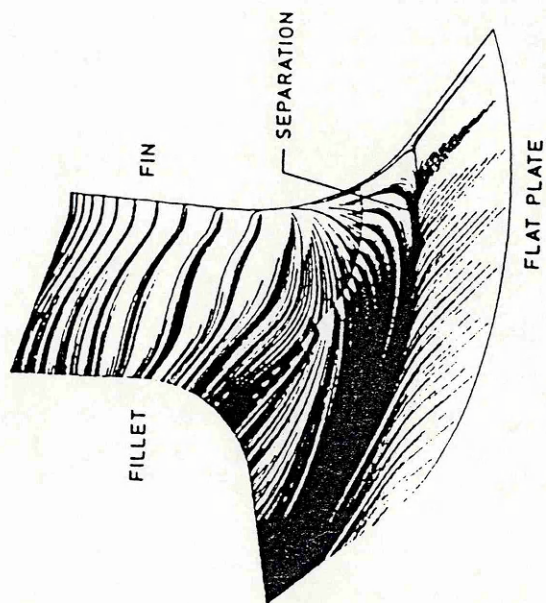
(b) Shizawa's porous plate (holes) (Ref.57)

Fig.1-11 Examples of air injection/suction techniques

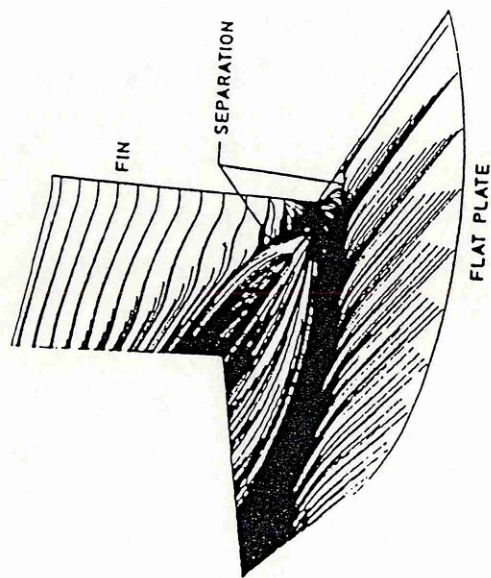


(c) A successful effect of air suction on the interaction (Ref.57)

Fig.1-11 Continue

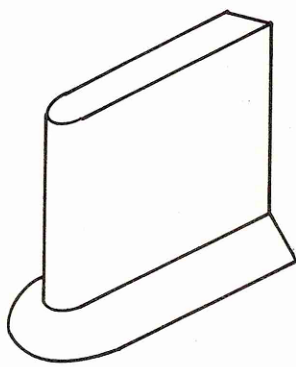


(a) UNMODIFIED

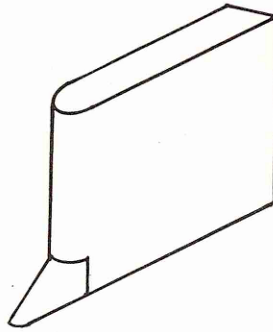


(b) WITH FILLET

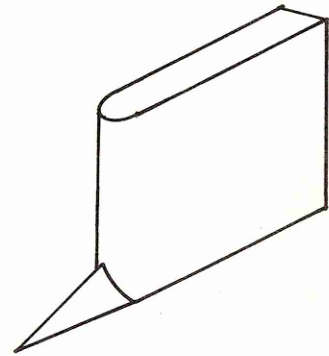
Fig.1-12 Lakshmanan's numerical models and surface flow patterns
(Ref.61)



Fillet

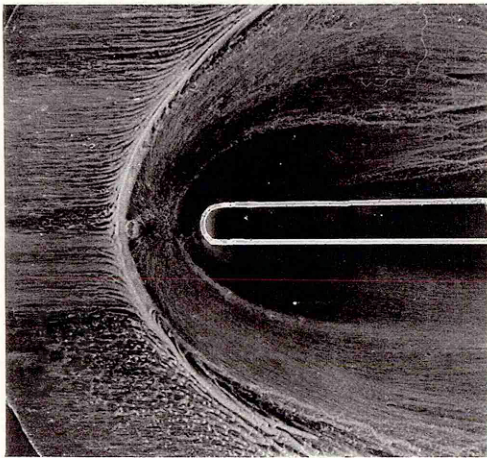


Blunt strake

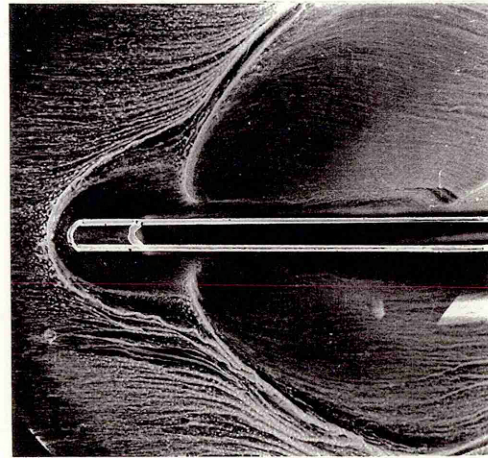


Sharp strake

(a) Blank's modified blunt fins



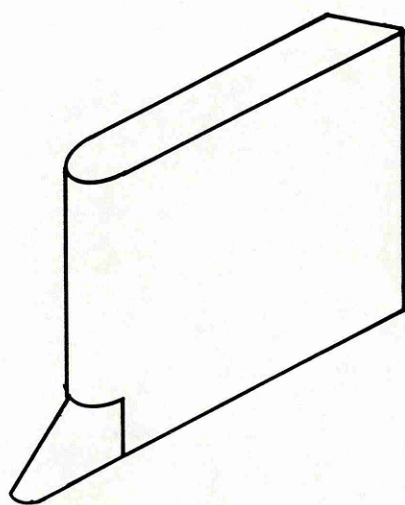
Unmodified



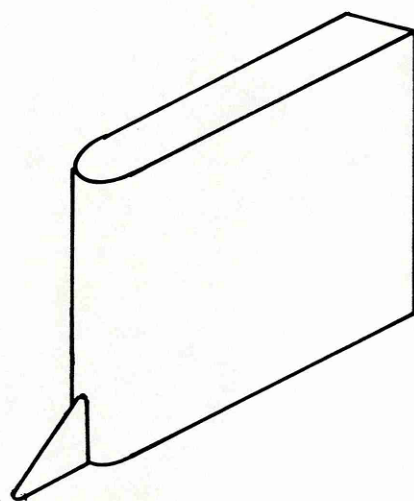
Blunt strake

(b) Oil streak patterns for unmodified and blunt strake (Ref.63)

Fig.1-13 Blank's modified blunt fins and oil streak patterns

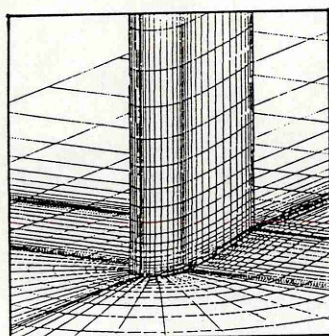


(a) Thick strake

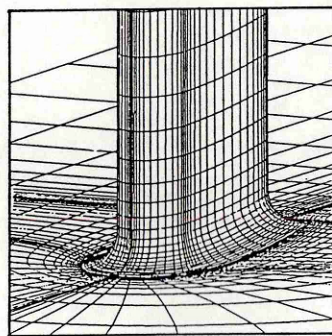


(b) Thin strake

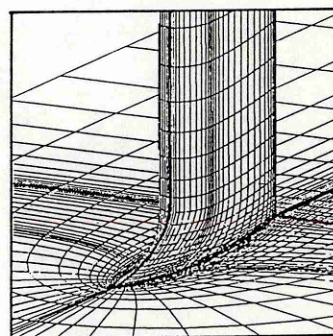
Fig.1-14 Kleifges' experimental models



Baseline

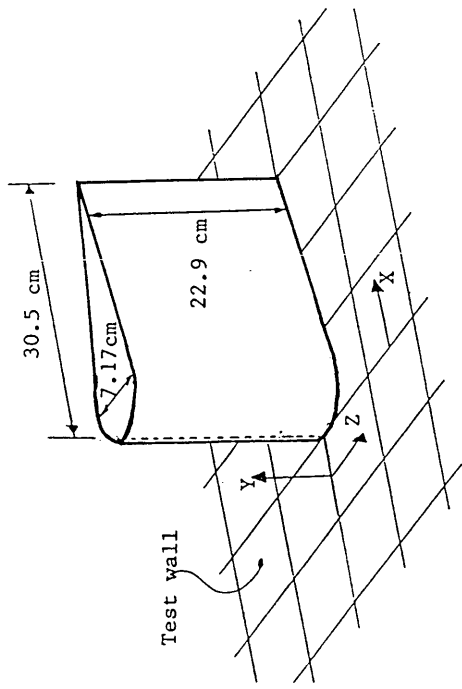


Fillet

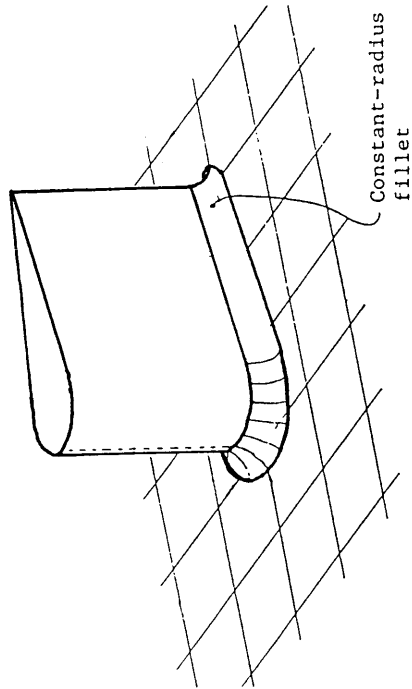


Strake

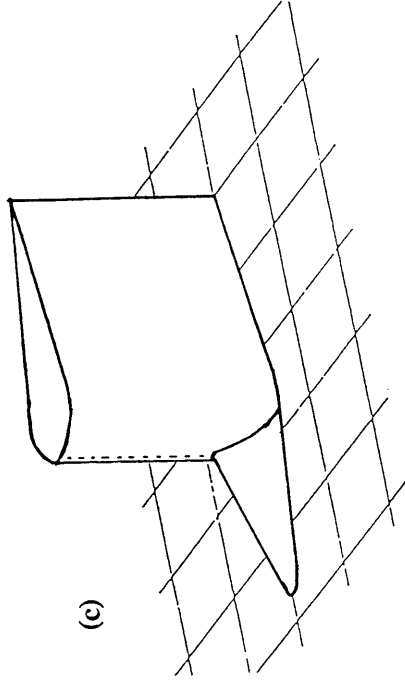
Fig.1-15 Sung's numerical junction-modified models (Ref.66)



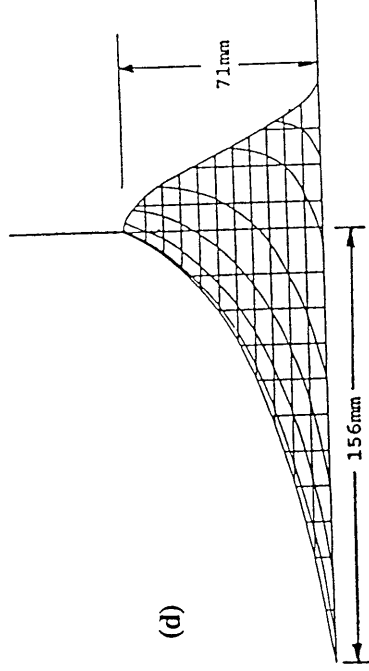
(a) baseline wing



(b) wing with fillet



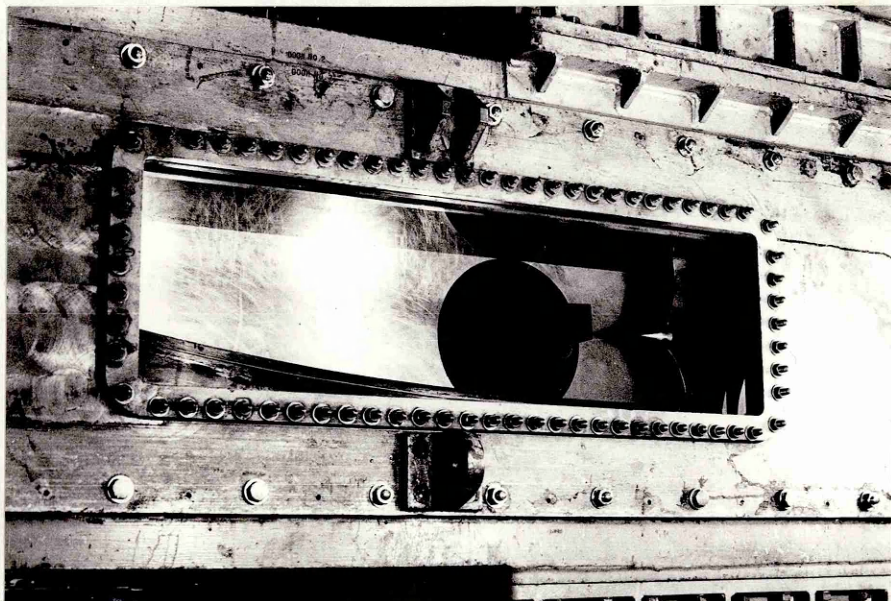
(c)



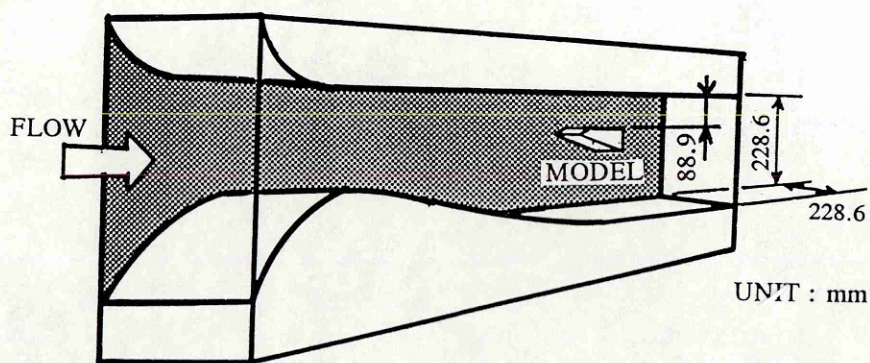
(d)

wing with strake
(c) perspective view
(d) side view of strake

Fig.1-16 Devenport's experimental models (Ref.68)



(a) 9"x9" wind tunnel test section



(b) Schematic view of the tunnel test section and test model

Fig.2-1 9"x9" continuous wind tunnel

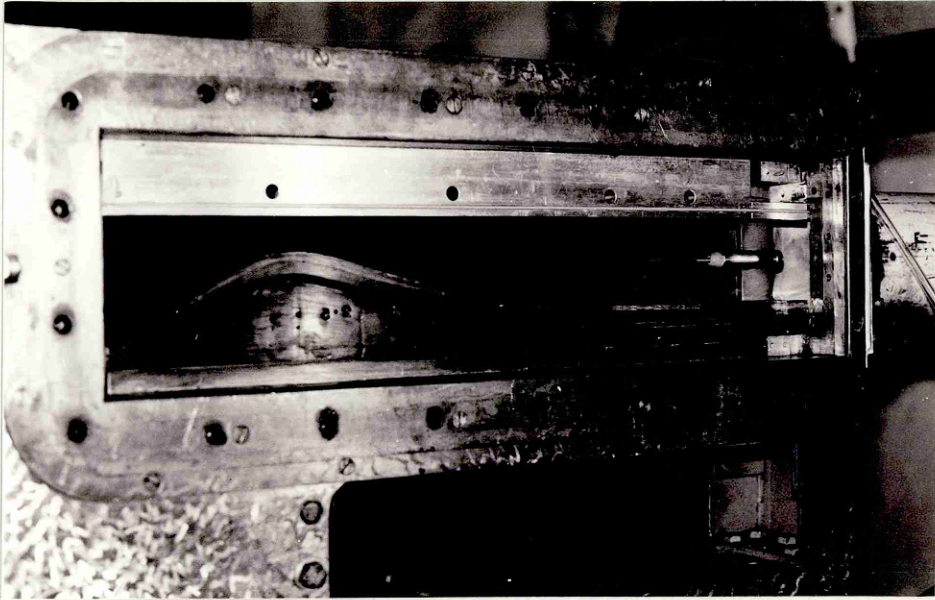
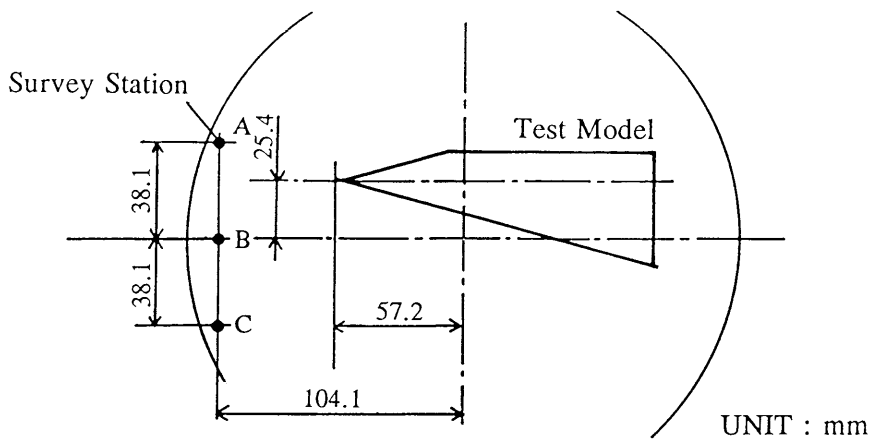
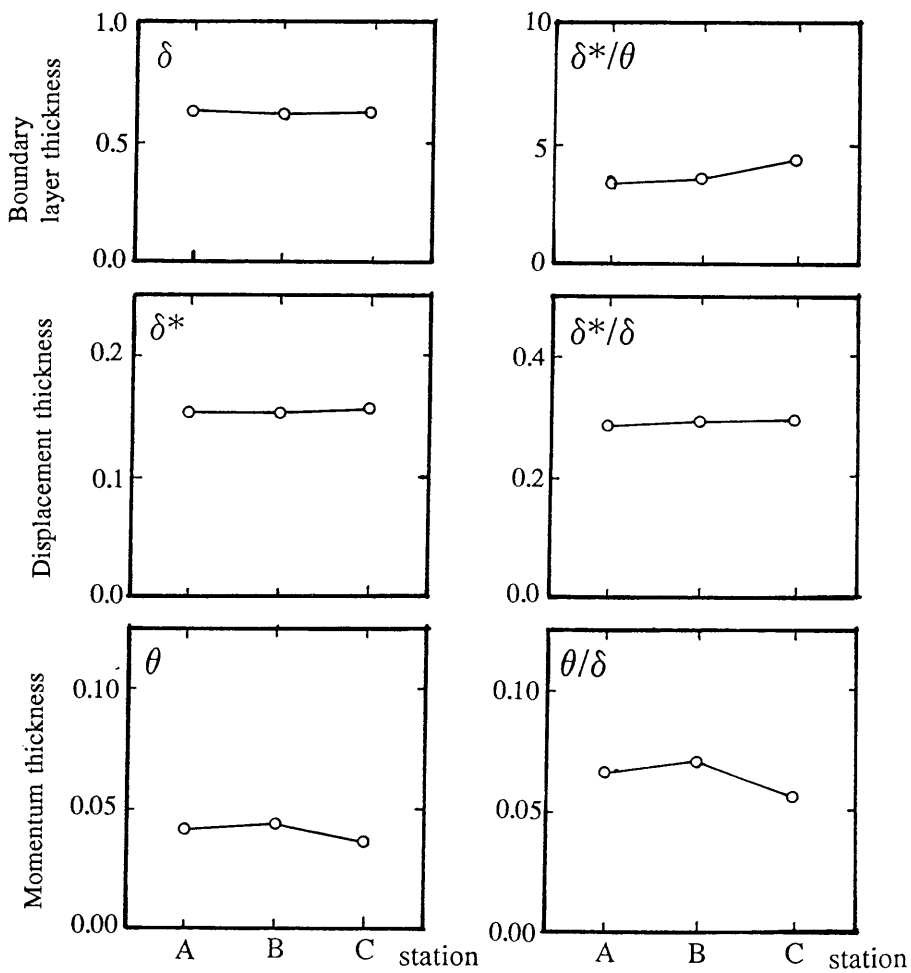


Fig.2-2 2.5"×2.5" indraft wind tunnel



(a) Survey stations



(b) Boundary layer properties

Fig.2-3 Boundary layer properties in the 9"×9" tunnel test section (Ref.12)

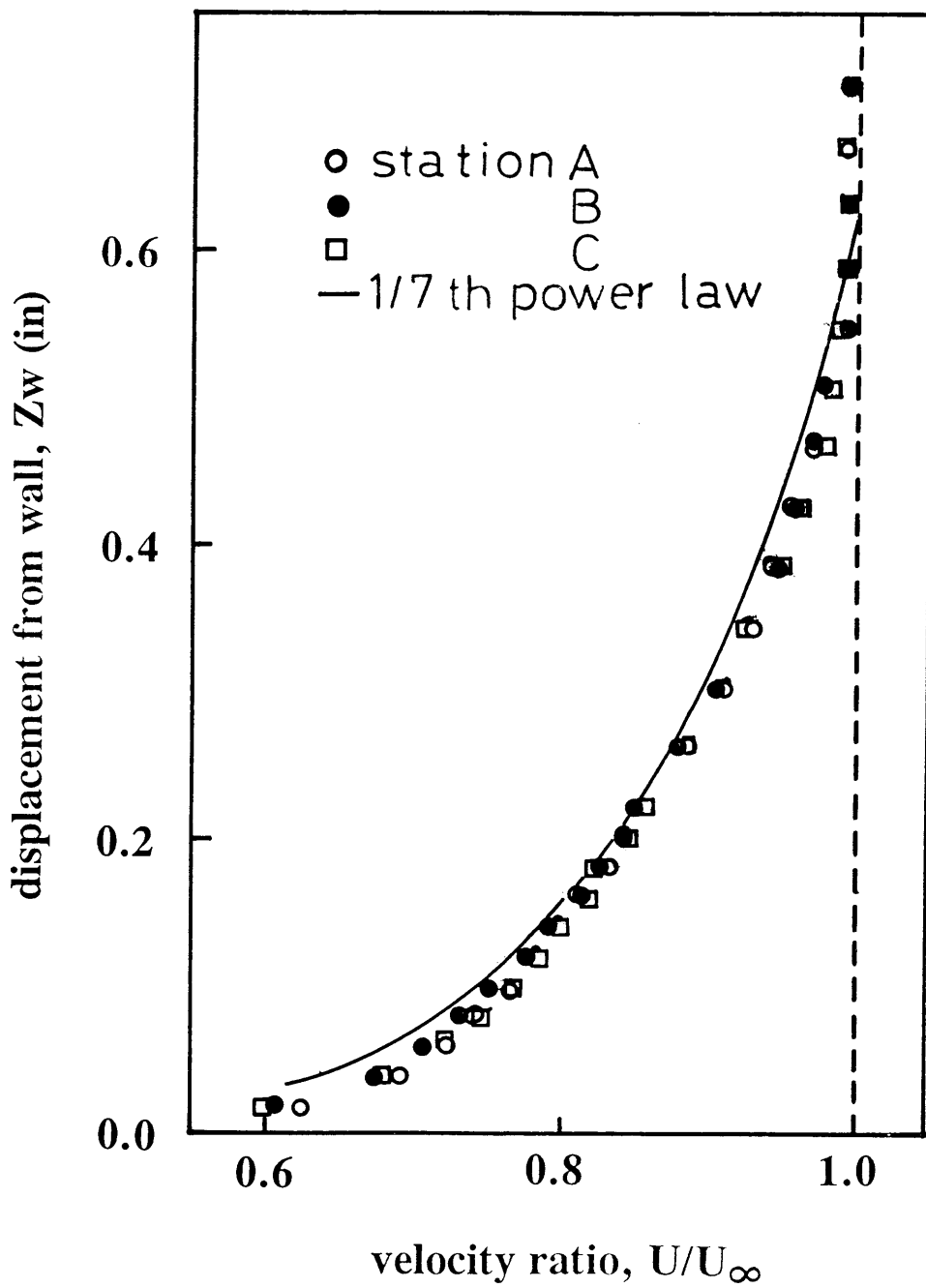
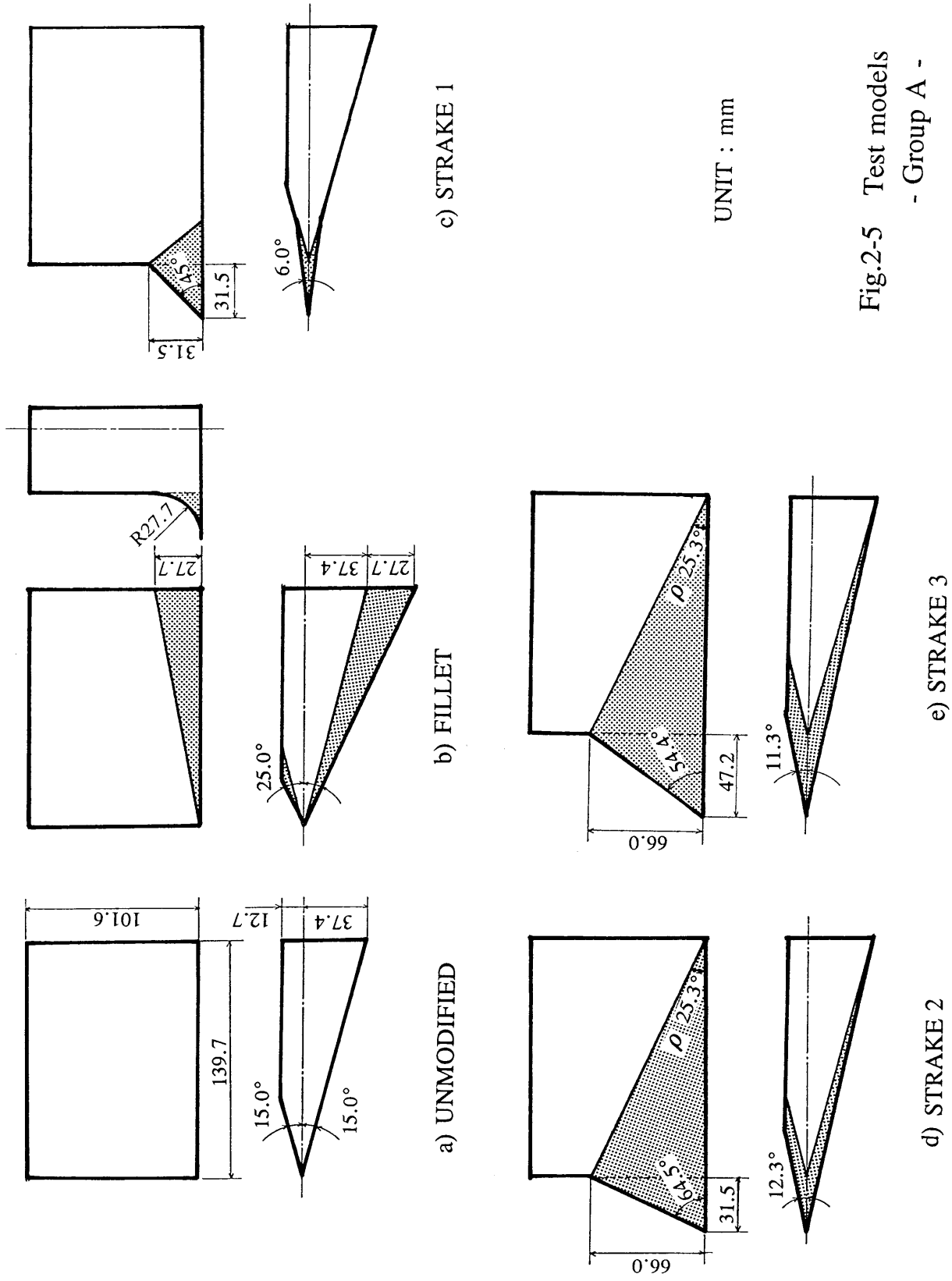
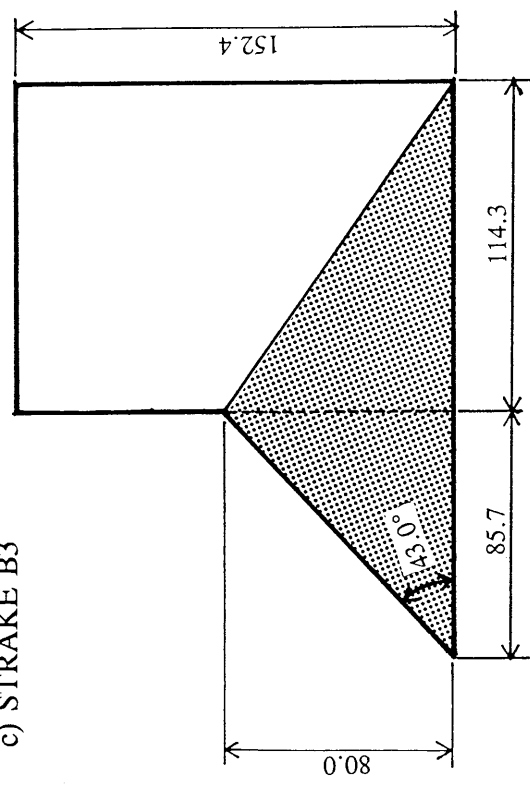
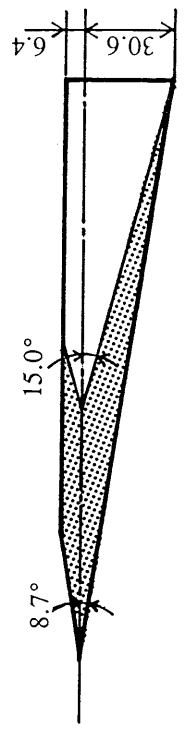


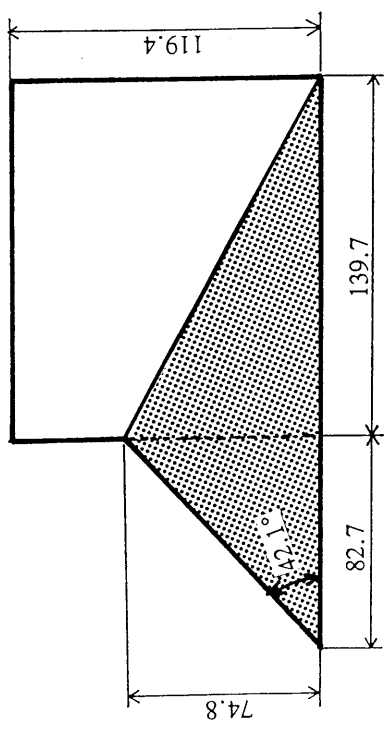
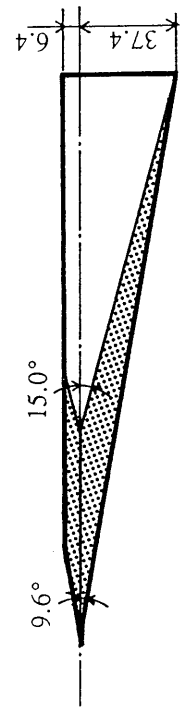
Fig.2-4 Velocity profiles of the boundary layer in the 9"×9" tunnel test section (Ref.12)



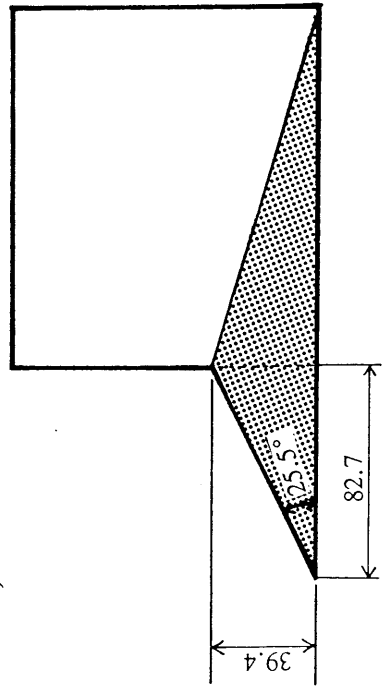
c) STRAKE B3



a) STRAKE B1

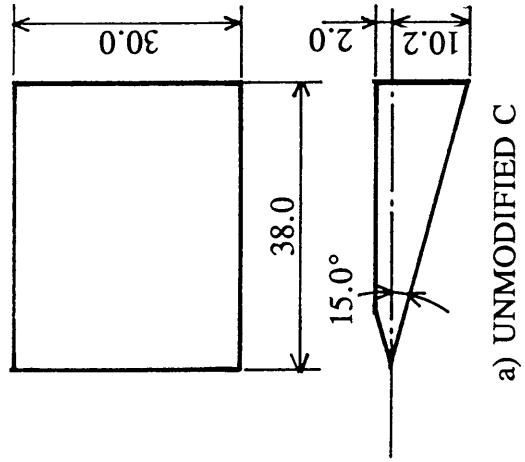
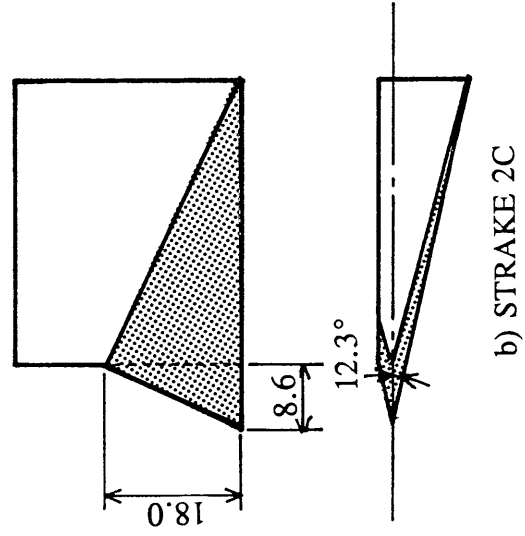
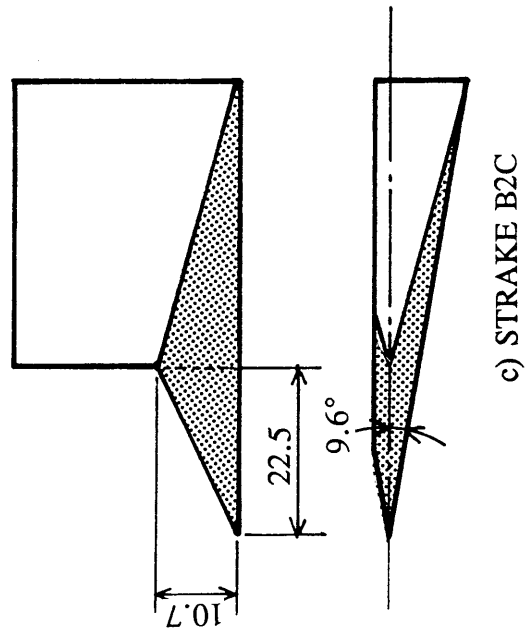


b) STRAKE B2



UNIT : mm

Fig.2-6 Test models
- Group B -



UNIT : mm

Fig.2-7 Test models
- Group C -

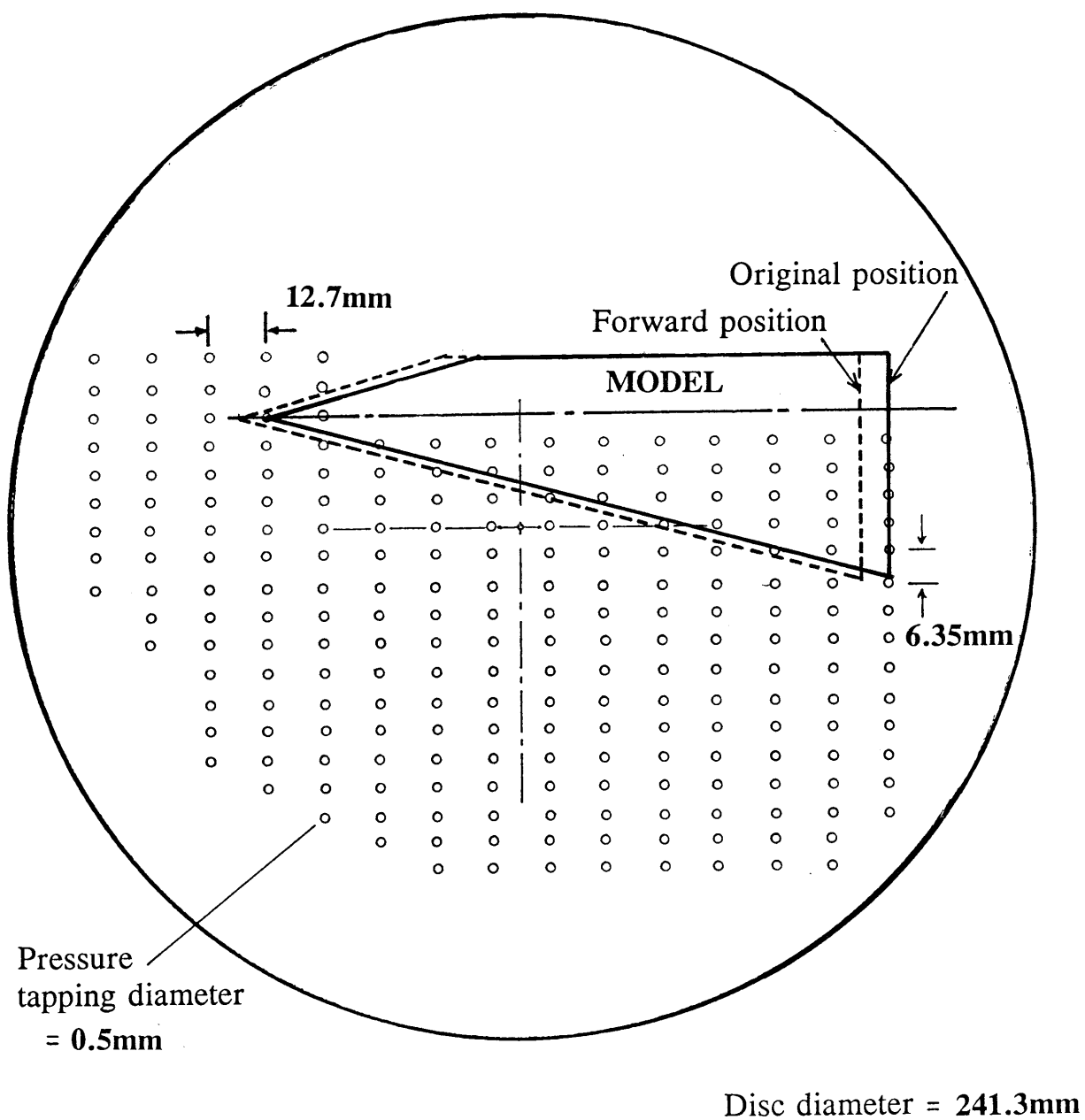


Fig.2-8 Distribution of pressure tapings on the pressure survey disk

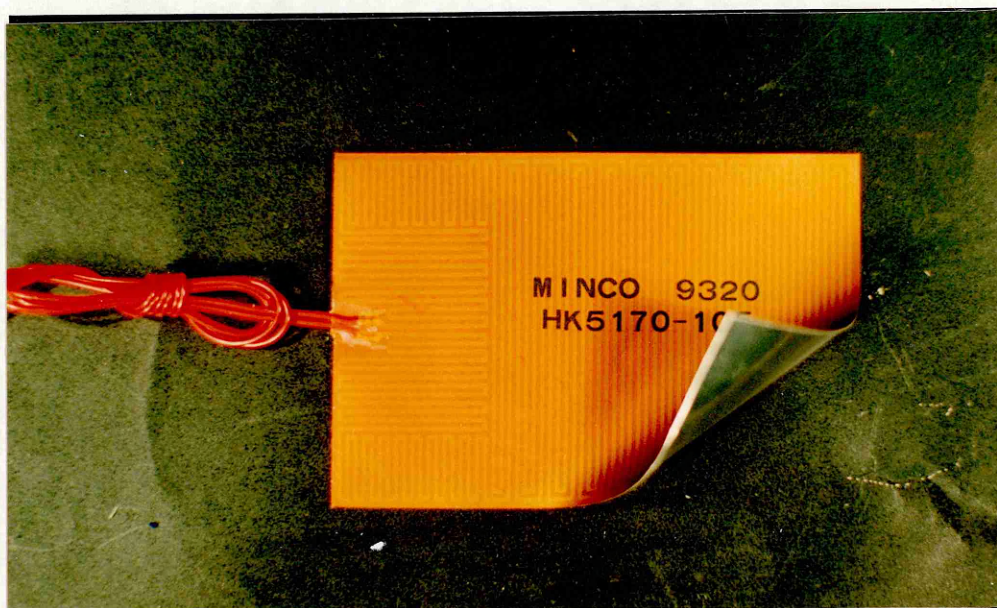


Fig.2-9 Thermofoil heater (Minco HK5170R123)

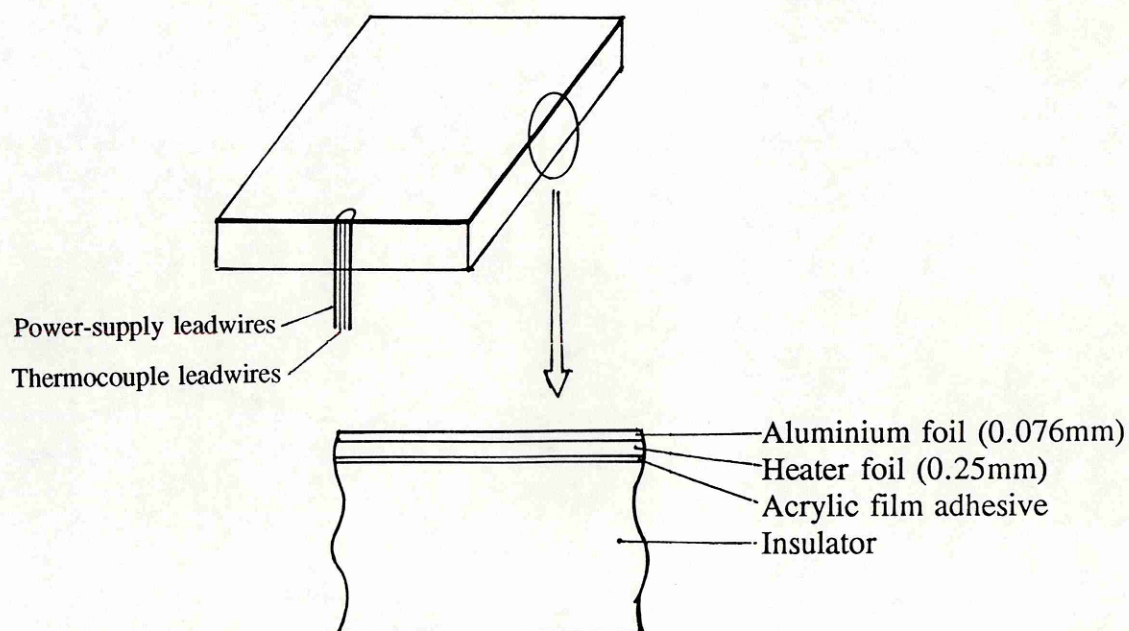


Fig.2-10 Heater assembly for the liquid crystal thermography tests

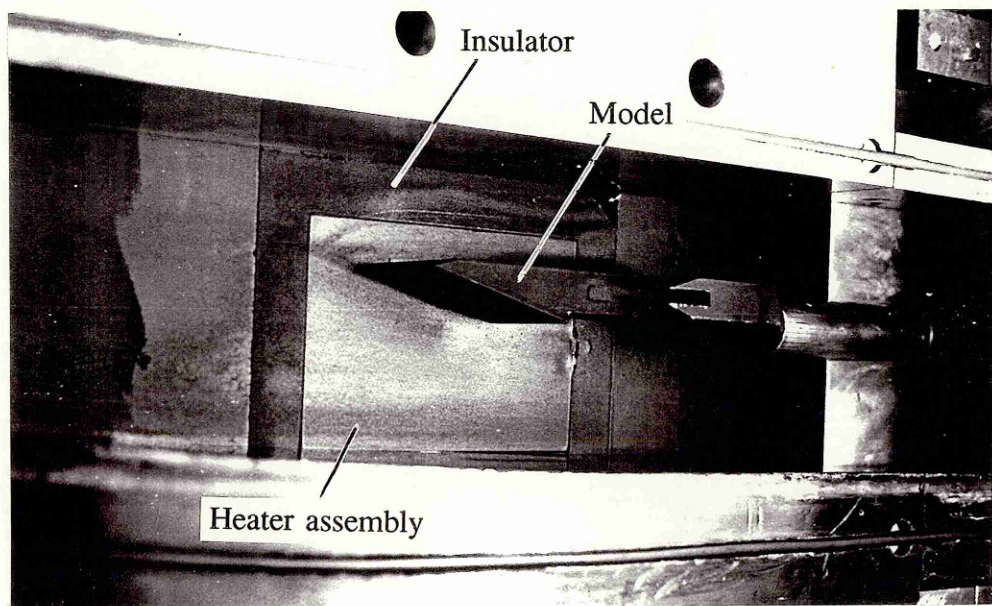
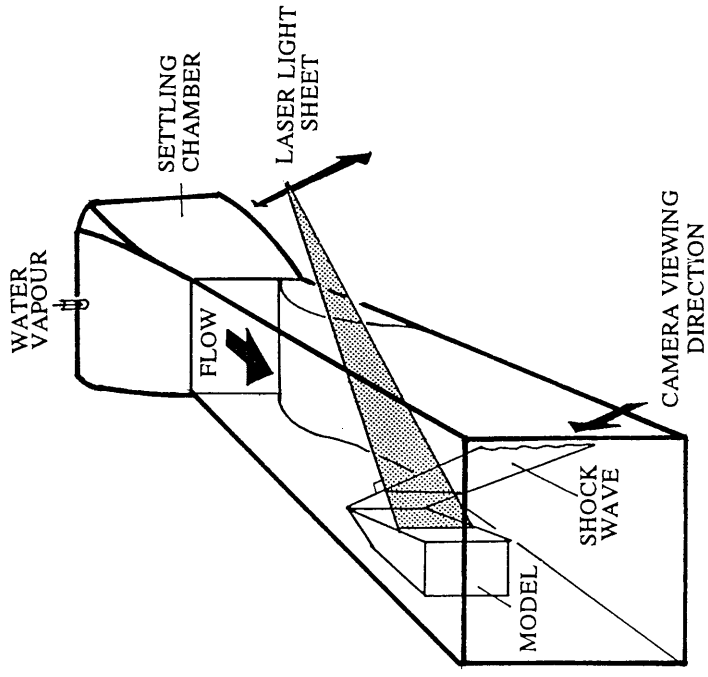
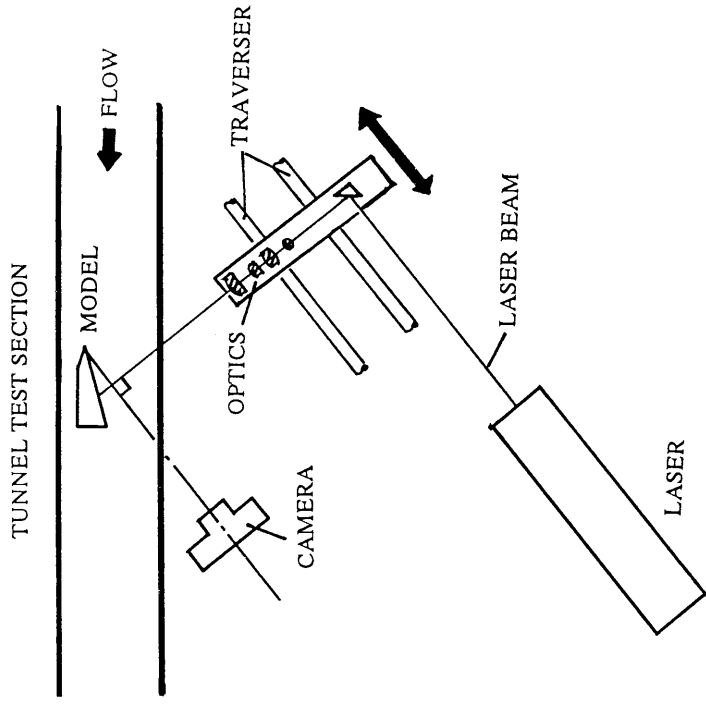


Fig.2-11 Test Arrangement for the liquid crystal thermography experiments



(a) Schematic view of the tunnel, the model and the light sheet



(b) Arrangement of each component of the LFV system

Fig.2-12 Test arrangement for the laser-light-sheet flow visualization (LFV)

Test model with white lines for
arranging the LFV system (see Page 20)

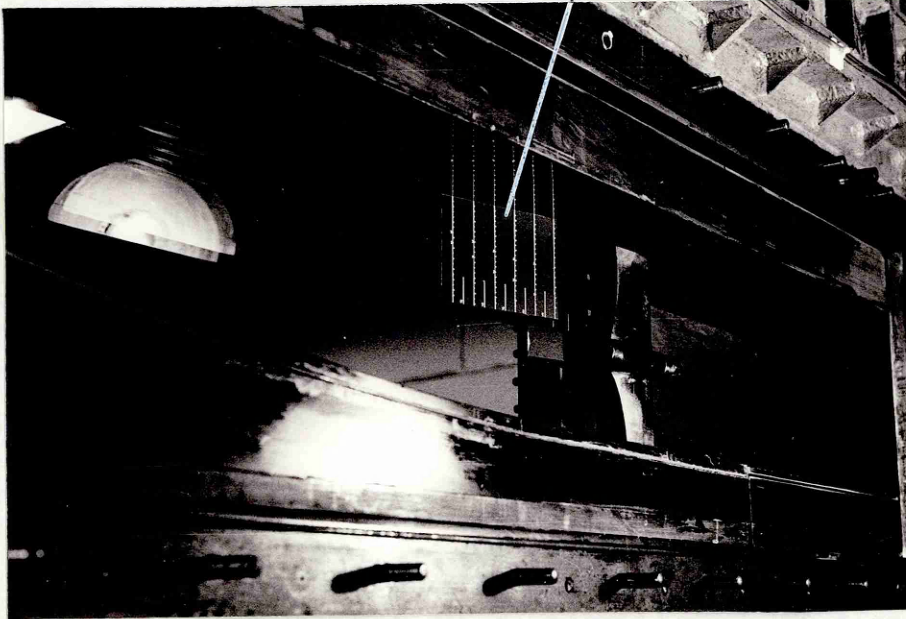


Fig.2-13 Model setting for the LFV testing

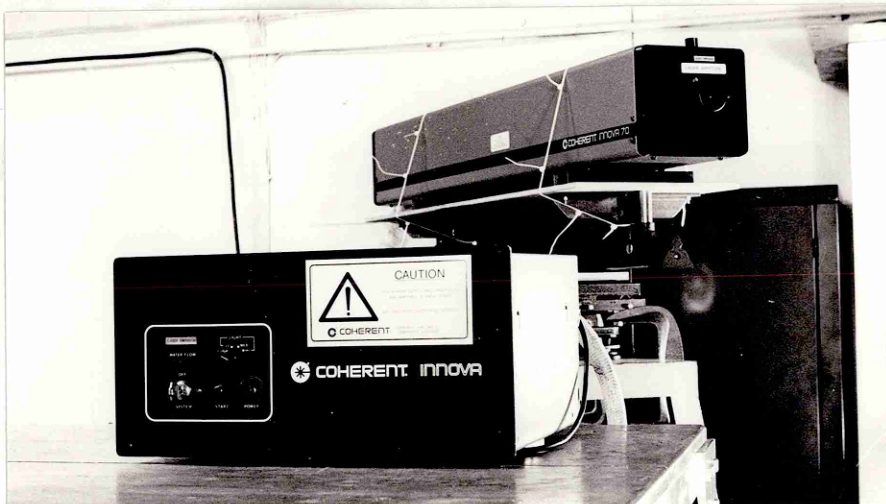


Fig.2-14 Argon-Ion laser employed for the LFV
(COHERENT INNOVA 70-4)

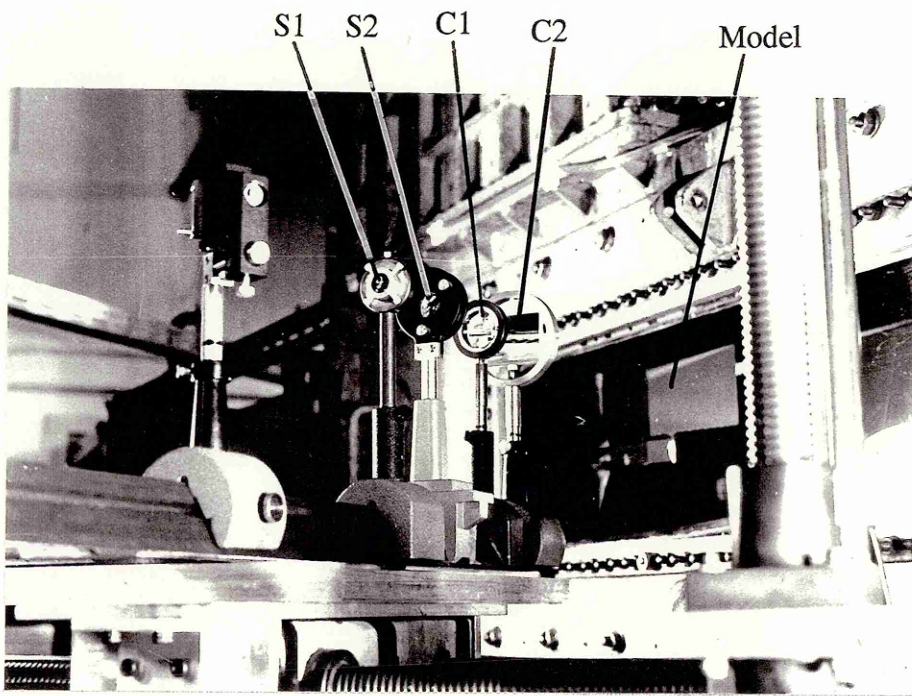
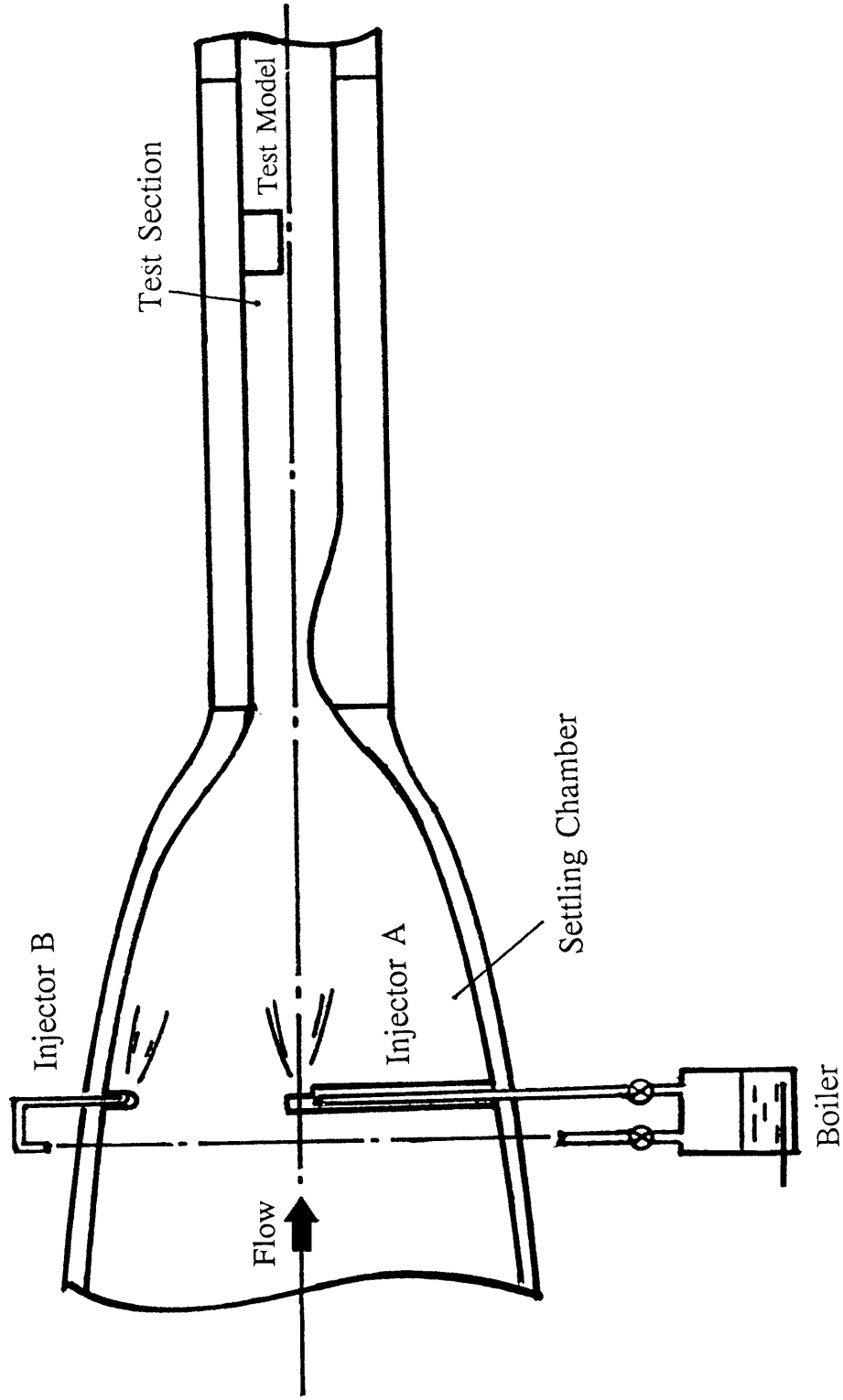
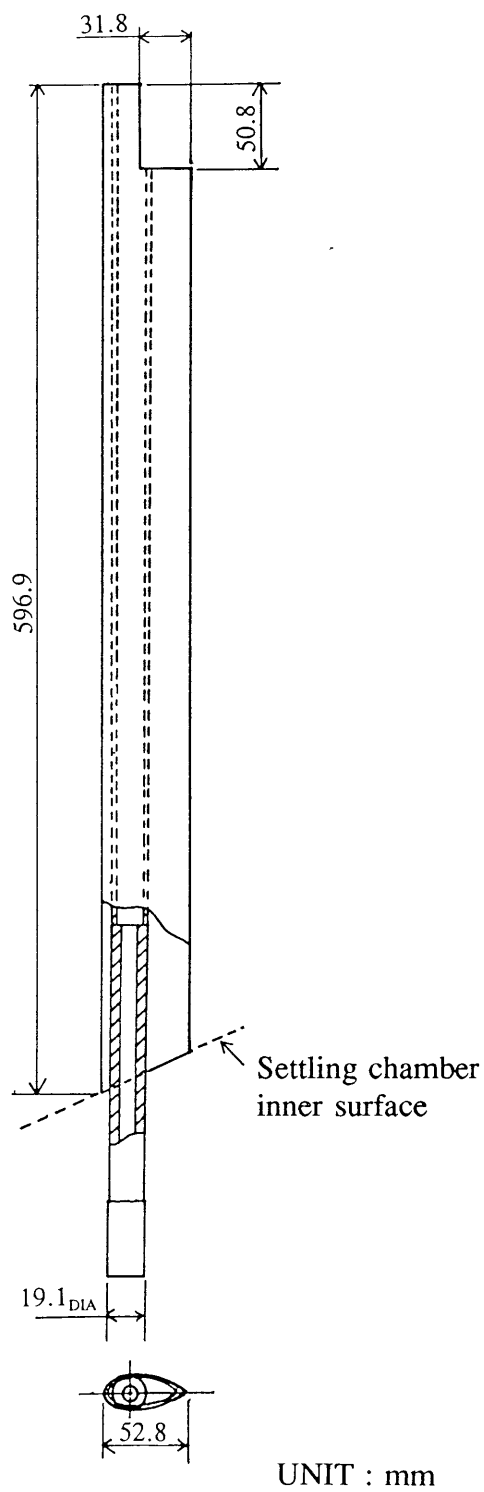


Fig.2-15 Laser-light-sheet generator placed on the traverser



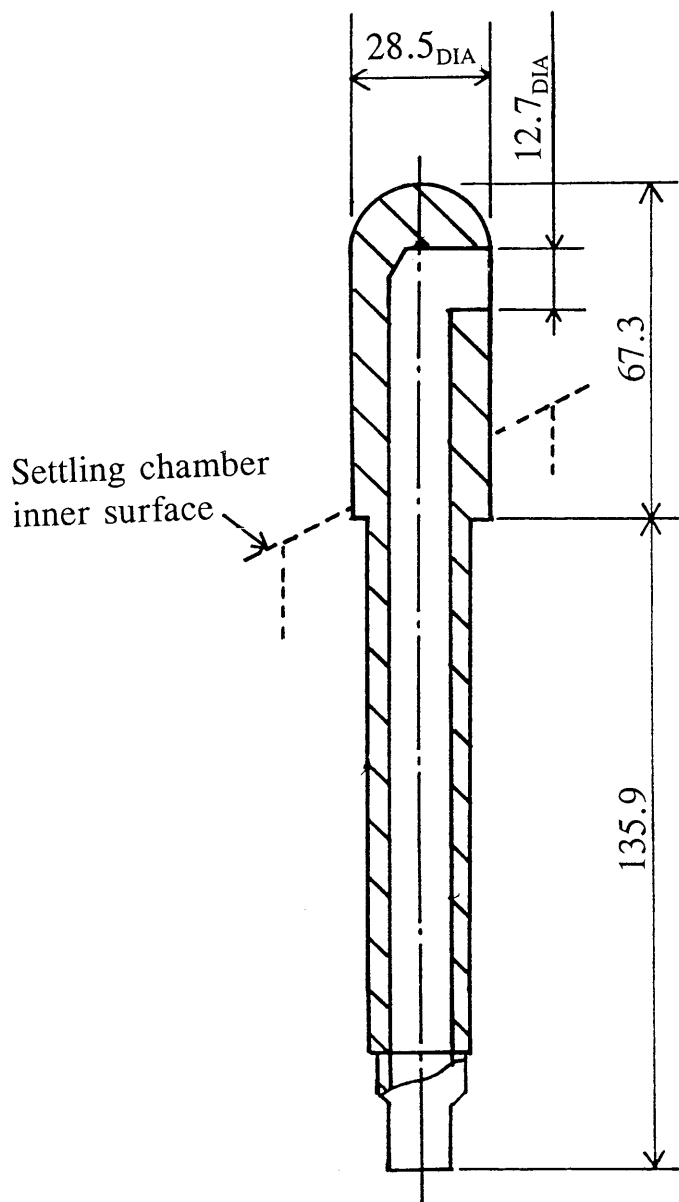
(a) Schematic view of the system

Fig.2-16 Vapour injection system



(b) Injector A

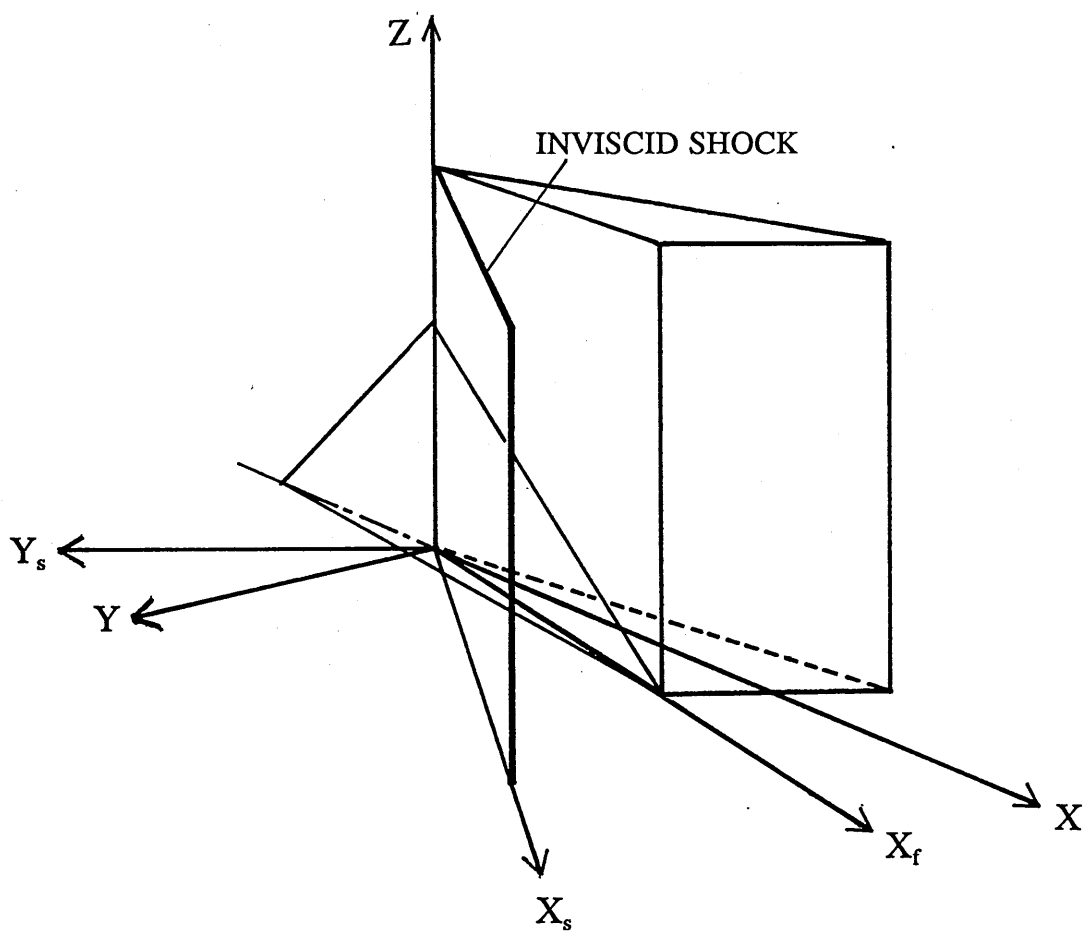
Fig.2-16 Continued



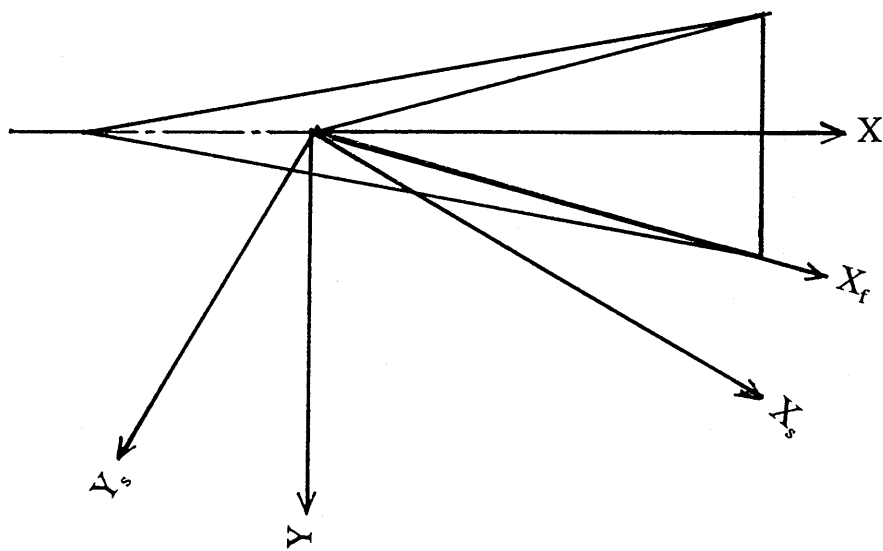
UNIT : mm

(c) Injector B

Fig.2-16 Continued



(a) Three-dimensional view



(b) Top view

Fig.2-17 Coordinate systems

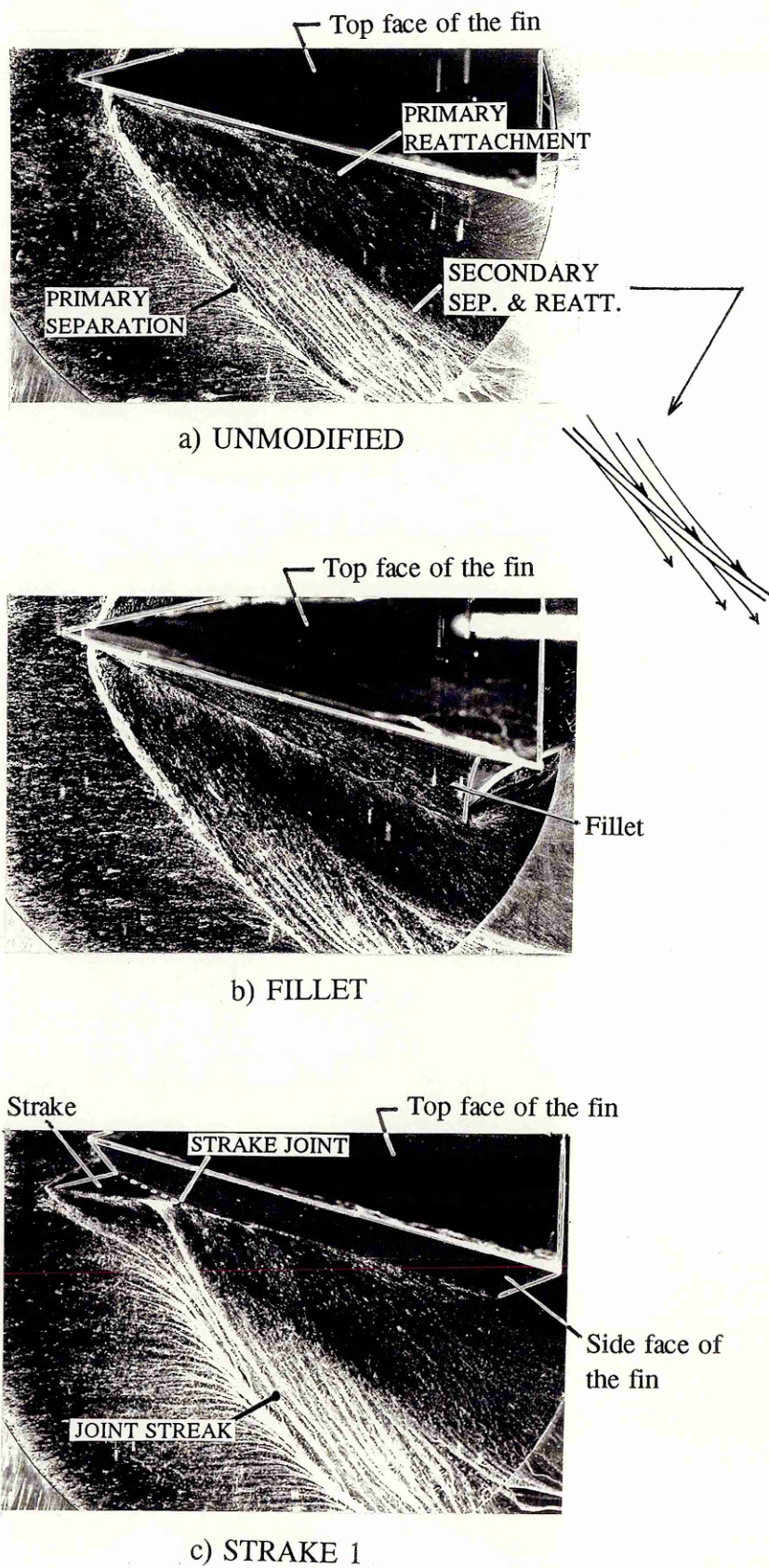
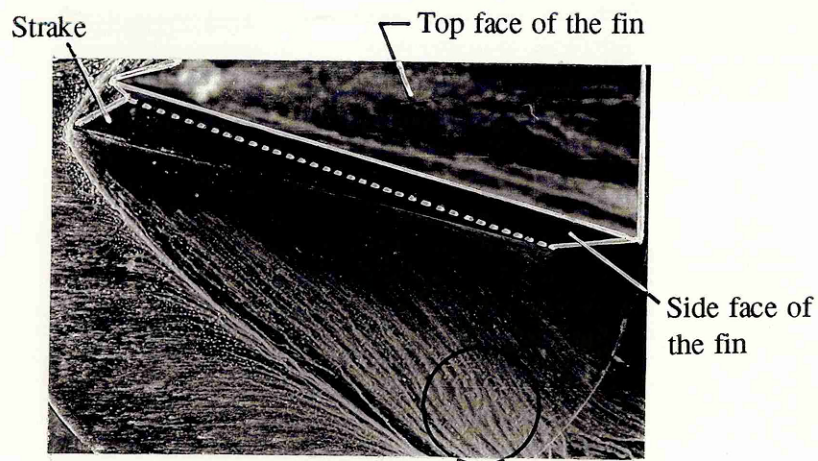
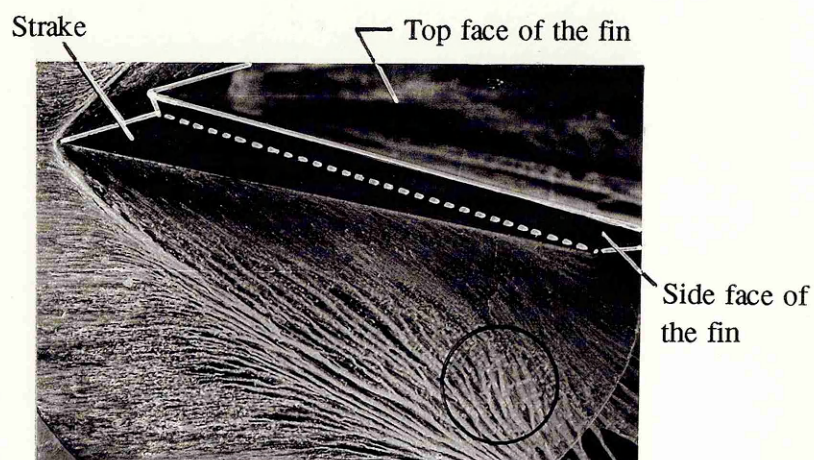


Fig.3-1 Oil flow patterns



d) STRAKE 2



e) STRAKE 3

Fig.3-1 Continued

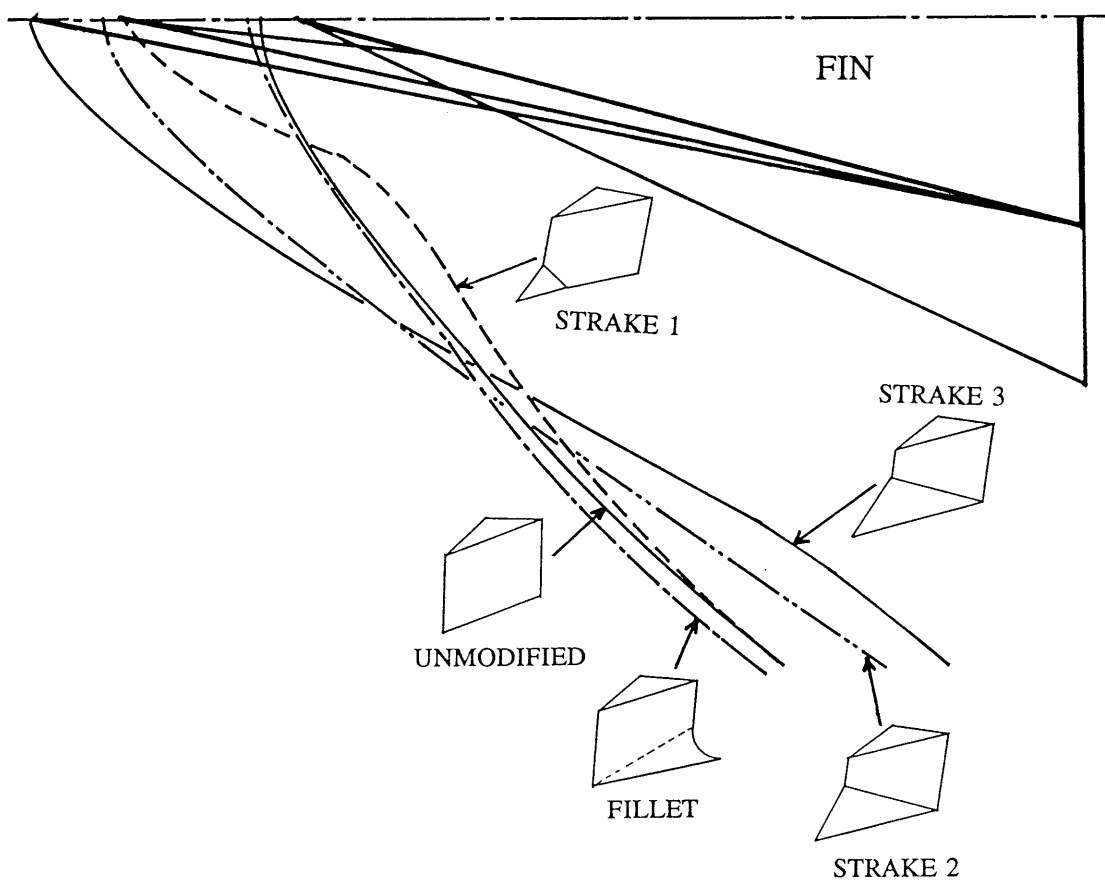
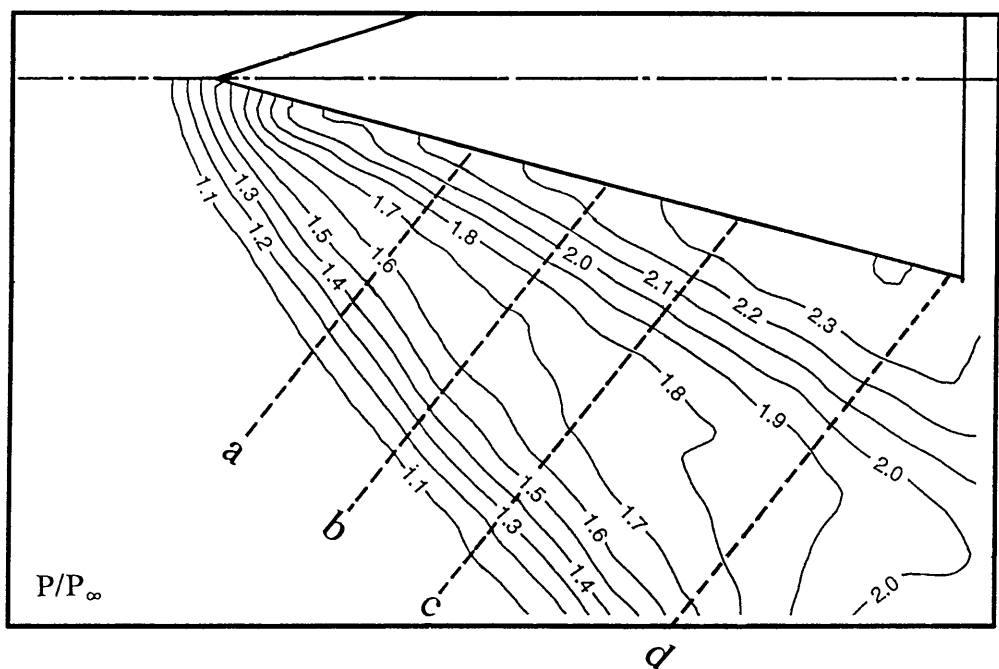
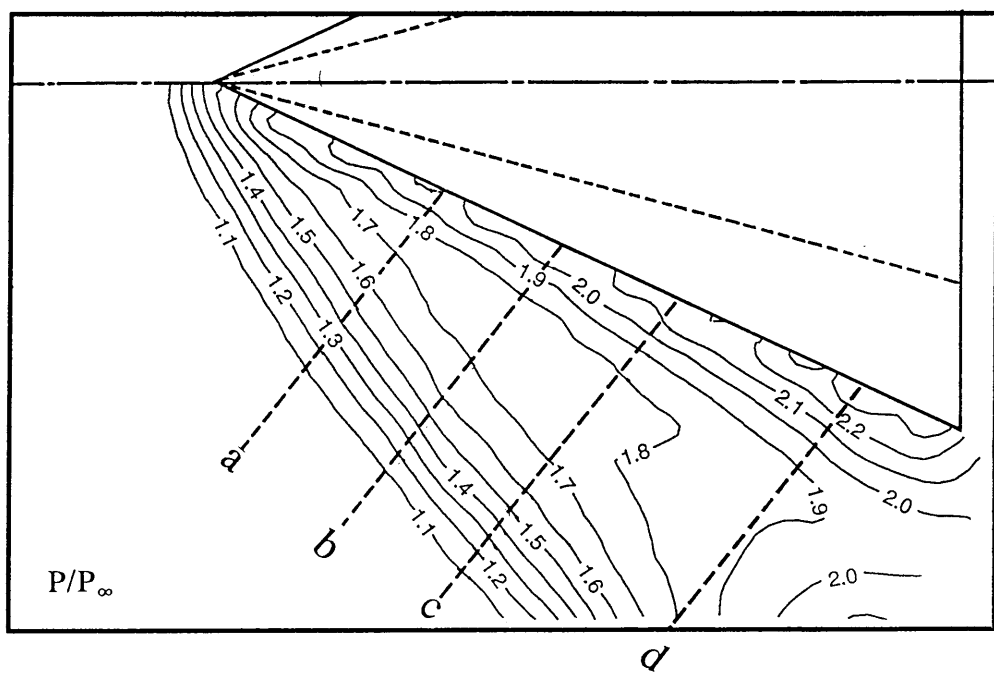


Fig.3-2 Positions of primary separation lines

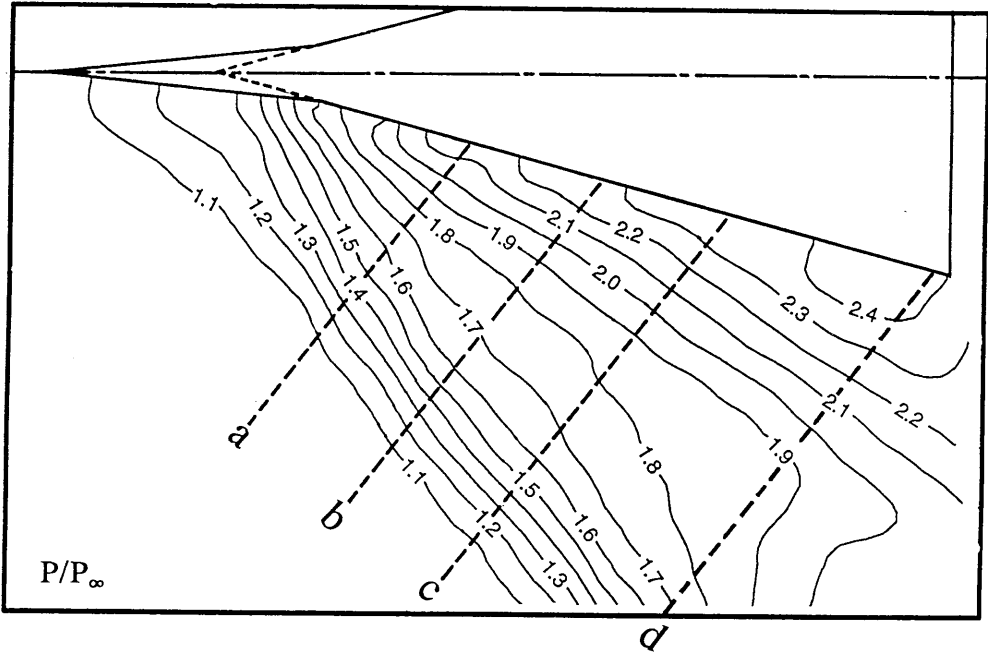


(a) Unmodified

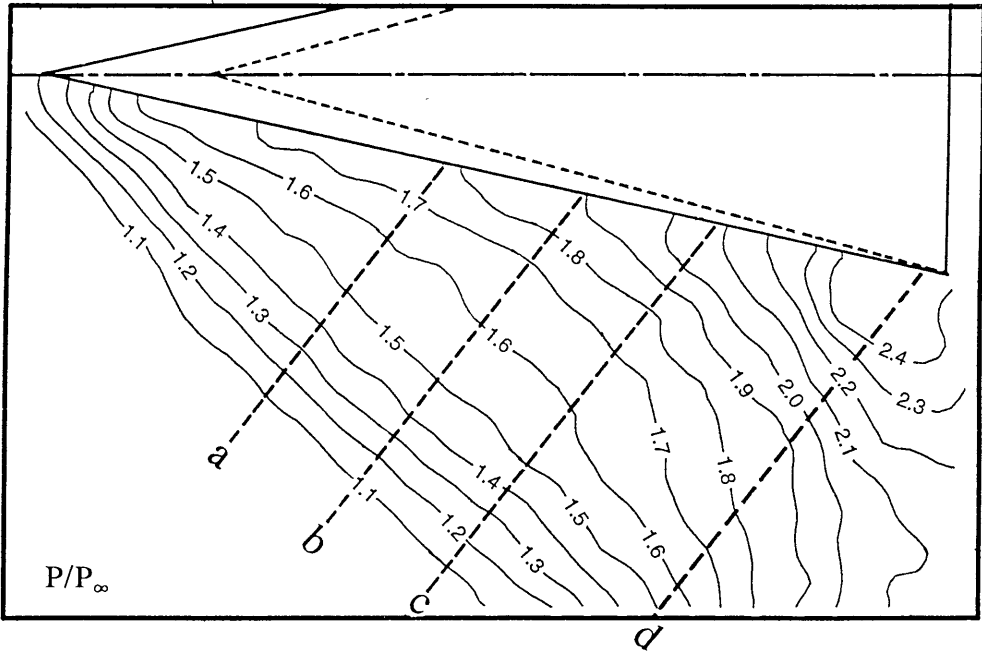


(b) Fillet

Fig.3-3 Isobar patterns on the wall

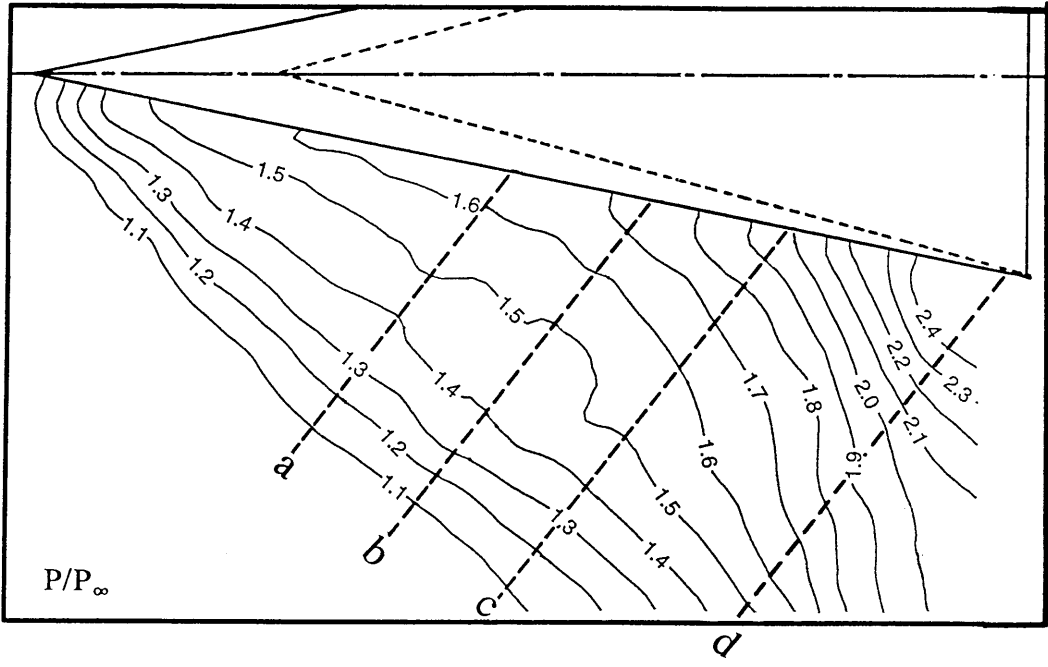


(c) Strake 1



(d) Strake 2

Fig.3-3 Continued



(e) Strake 3

Fig.3-3 Continued

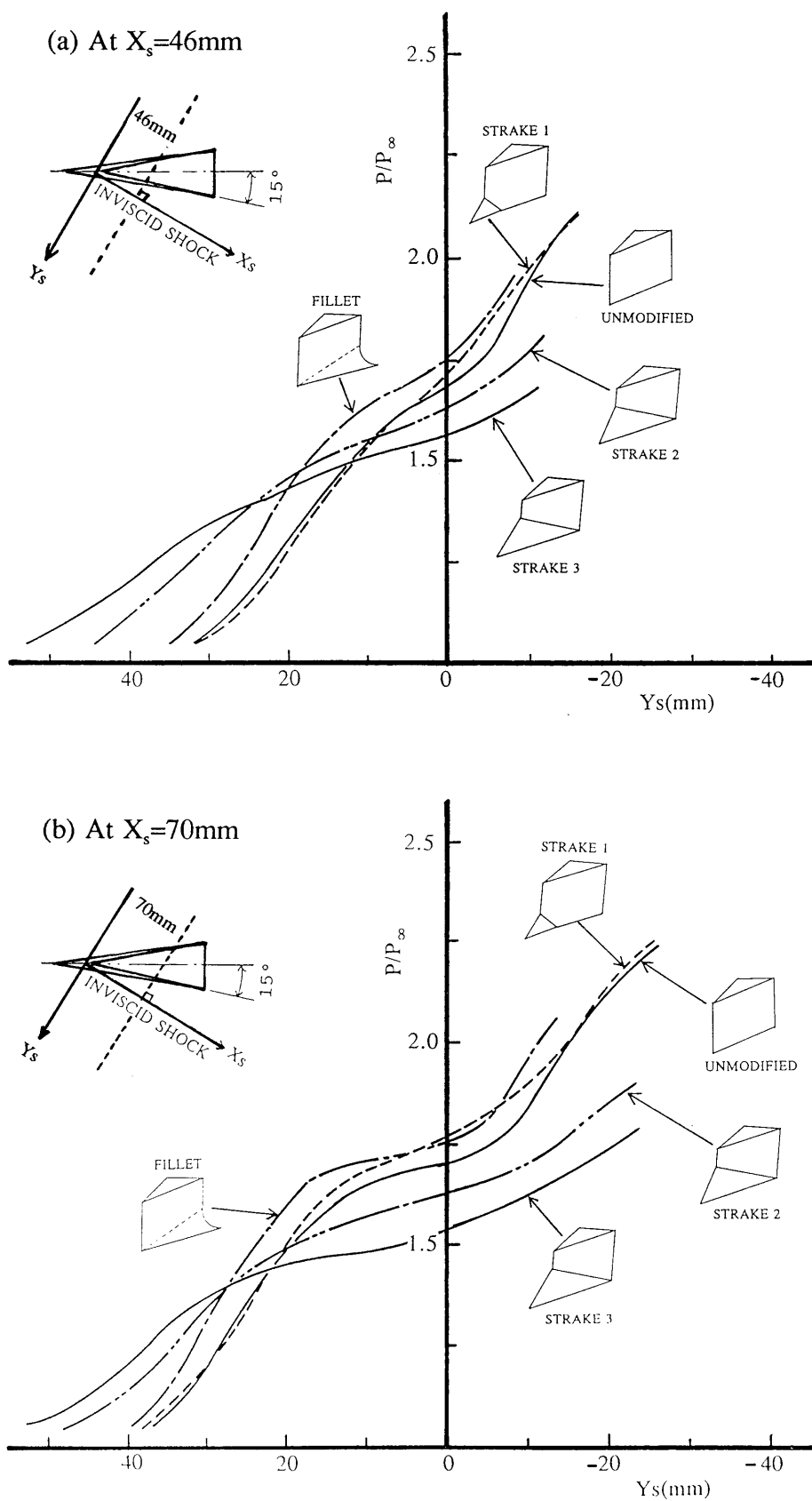


Fig.3-4 Wall pressure distributions in the direction perpendicular to the unmodified inviscid shock

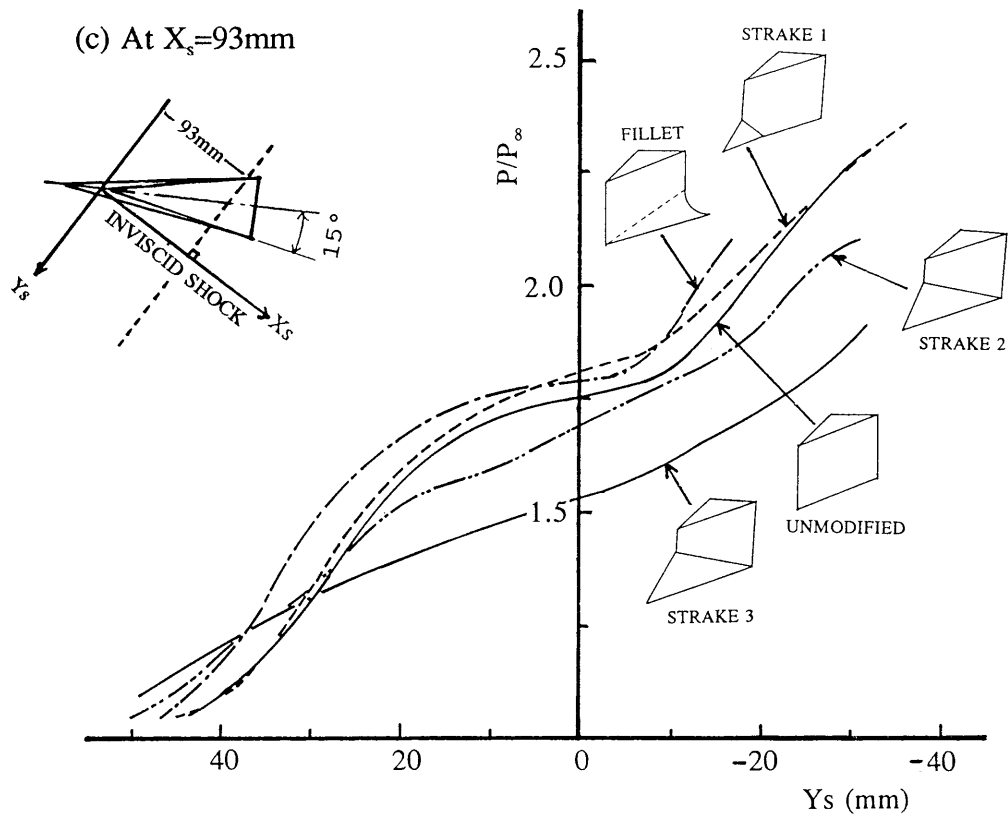
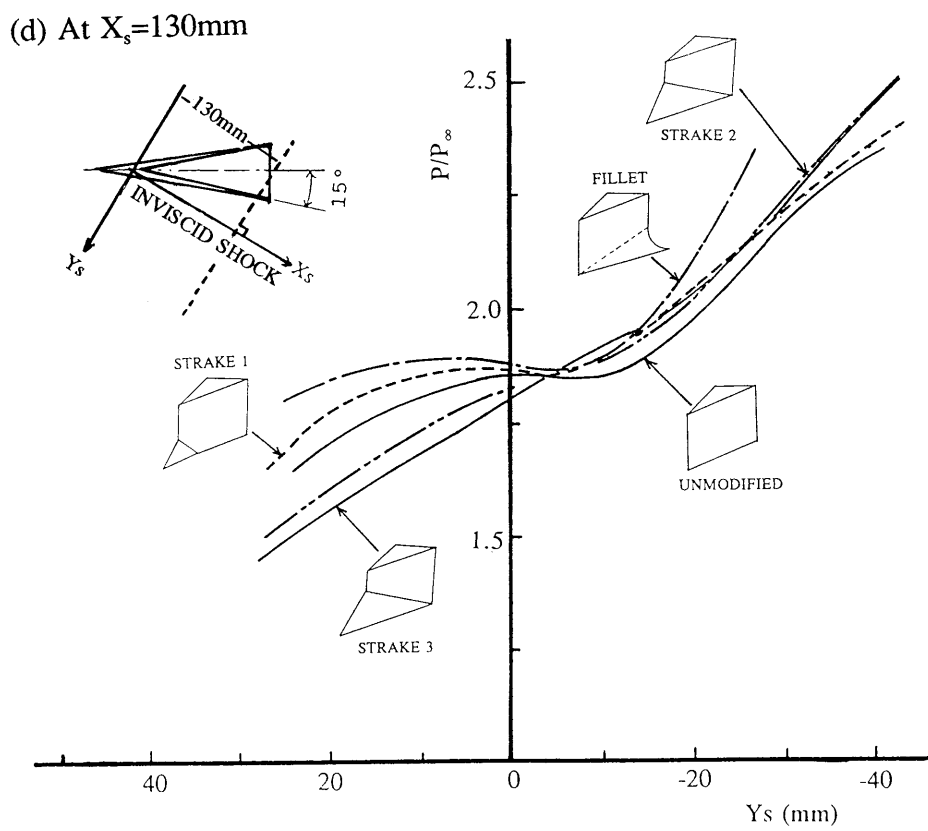
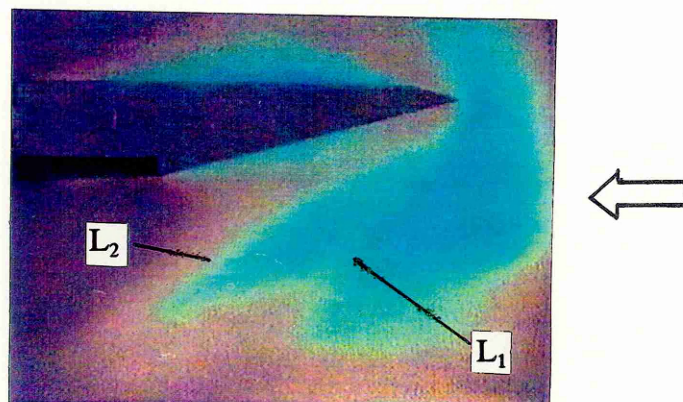
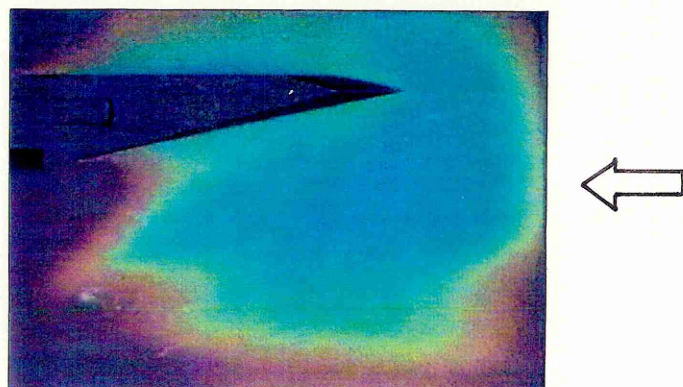


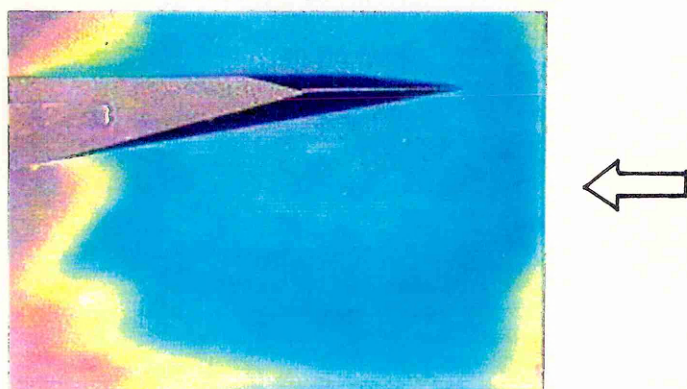
Fig.3-4 Continued



(a) Unmodified C

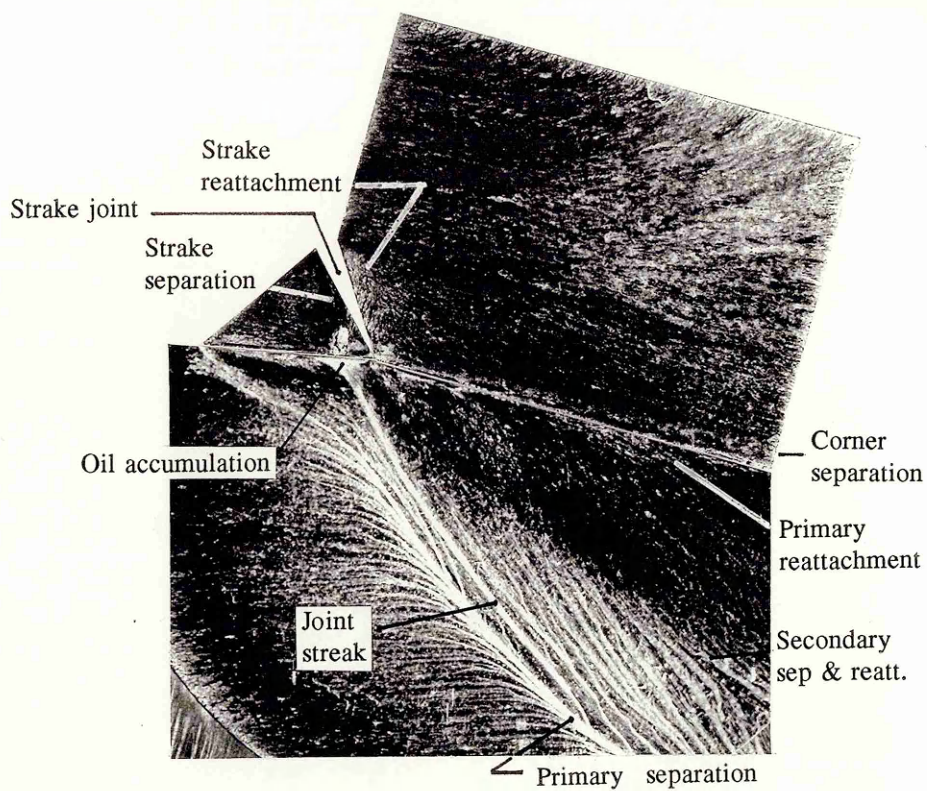


(b) Strake 2C

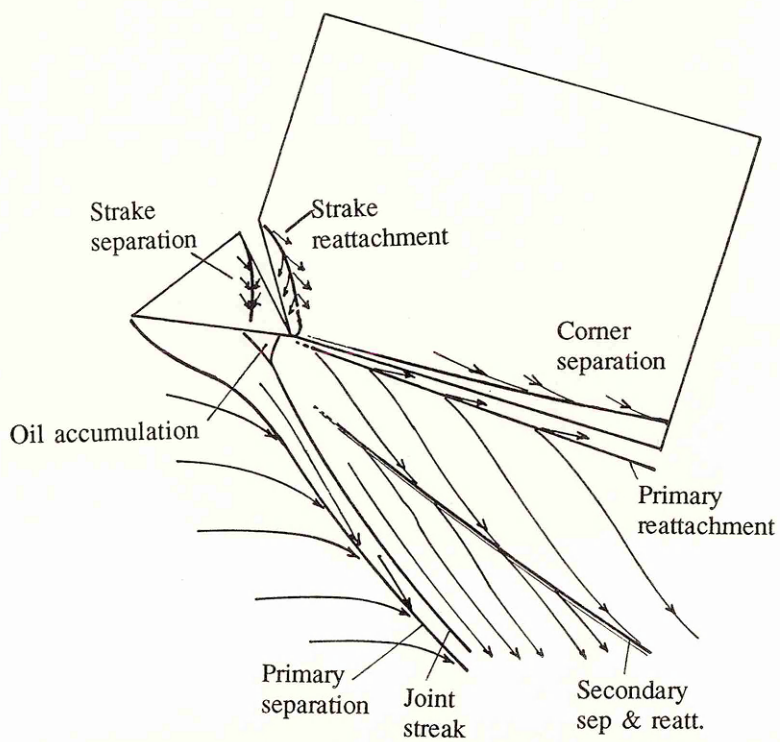


(c) Strake B2C

Fig.3-5 Liquid crystal thermal patterns on the wall

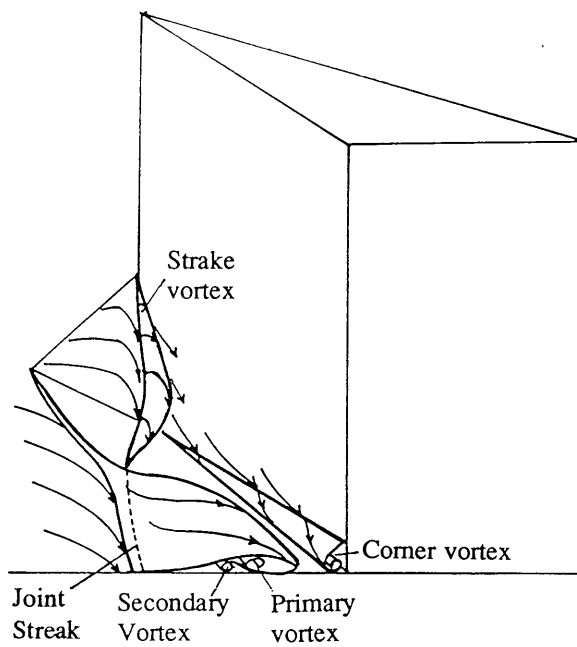


(a) Oil flow patterns on the wall and on the model surface



(b) Sketch of the oil streaks

Fig.3-6 Surface streak pattern and vortical flow structure of Strake 1



(c) Vortical flow structure

Fig.3-6 Continued

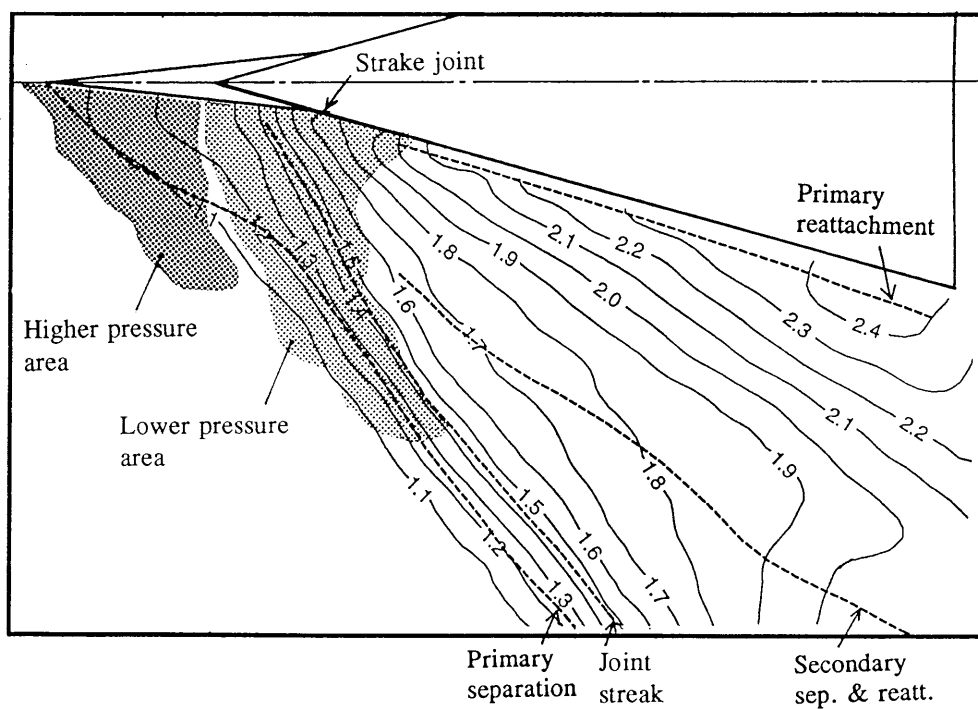


Fig.3-7 Isobar patterns and locations of the main surface flow features of Strake 1

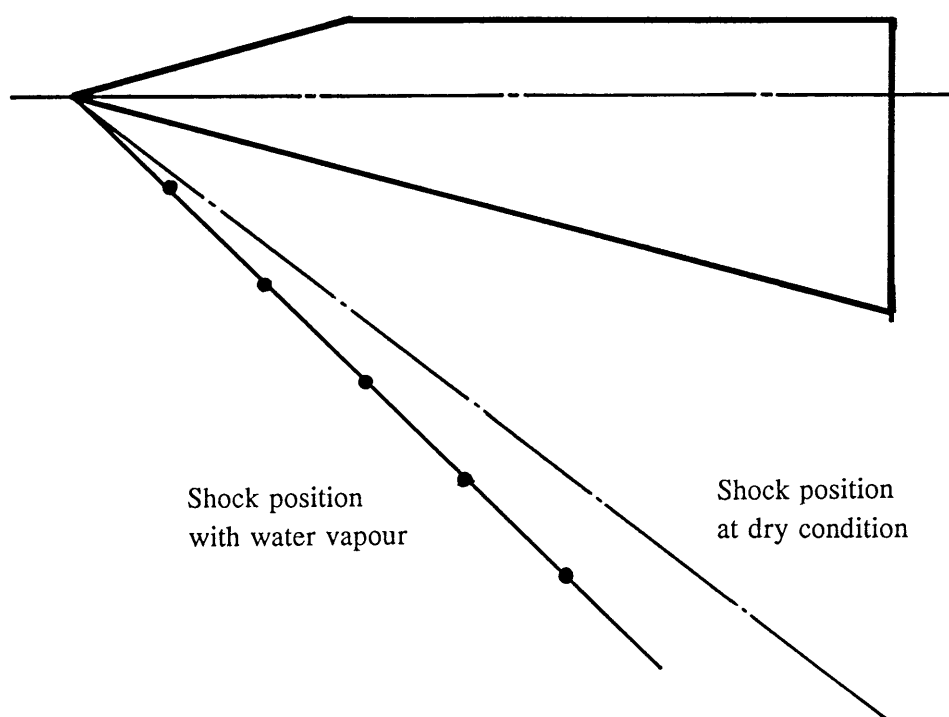


Fig.4-1 Deviation of the shock location caused by a condensation shock

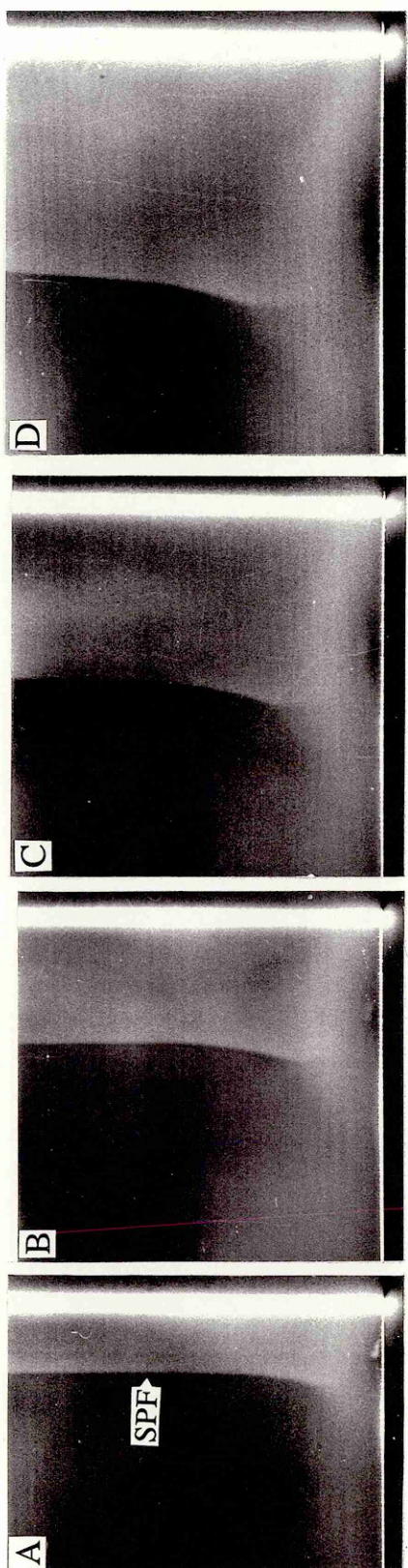
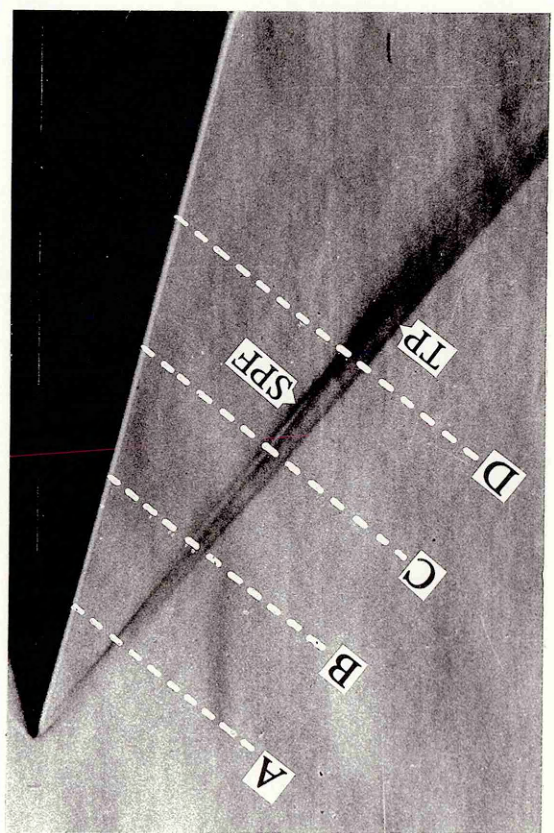
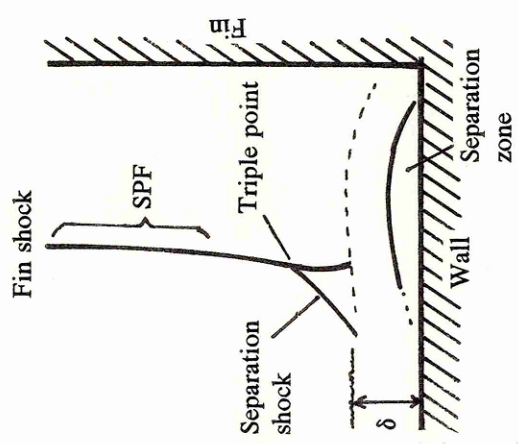


Fig.4-2 Schlieren and LFV images (a) Unmodified

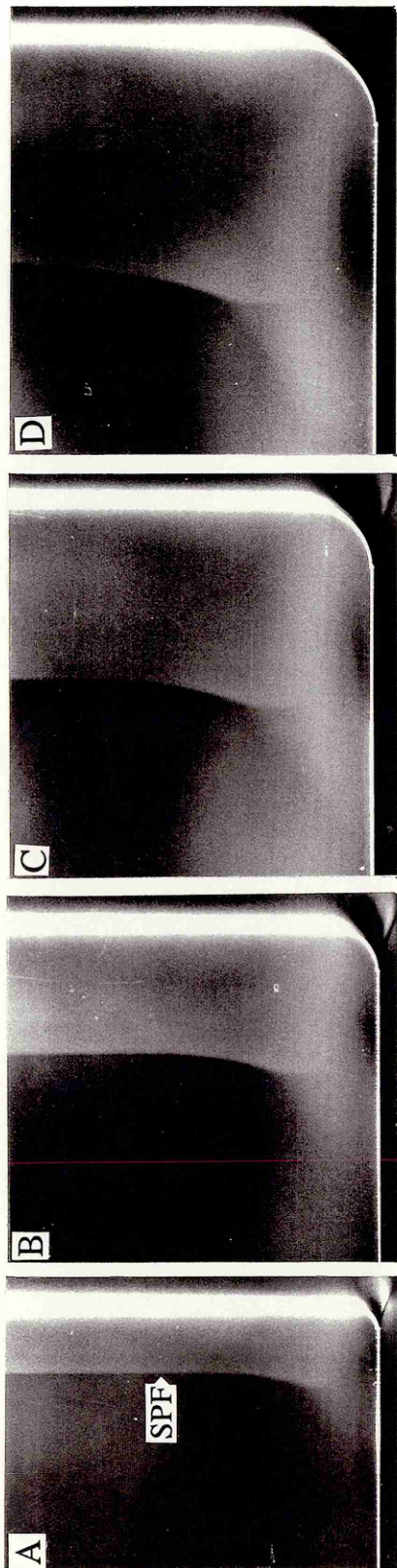
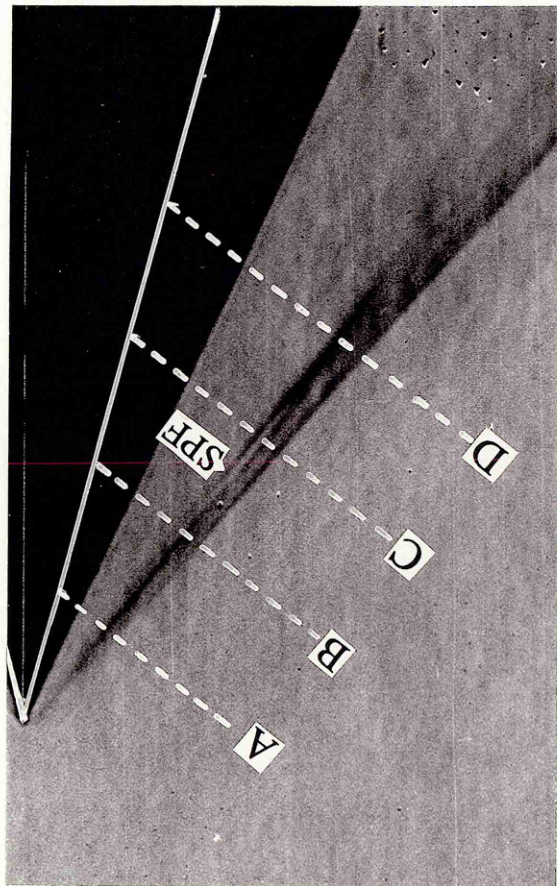


Fig.4-2 Continued (b) Fillet

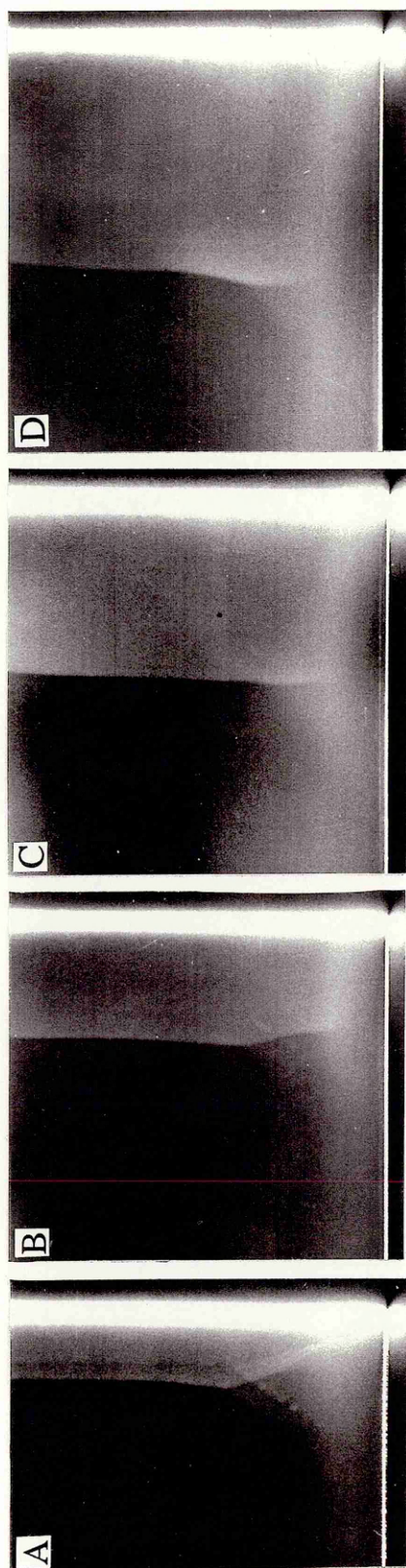
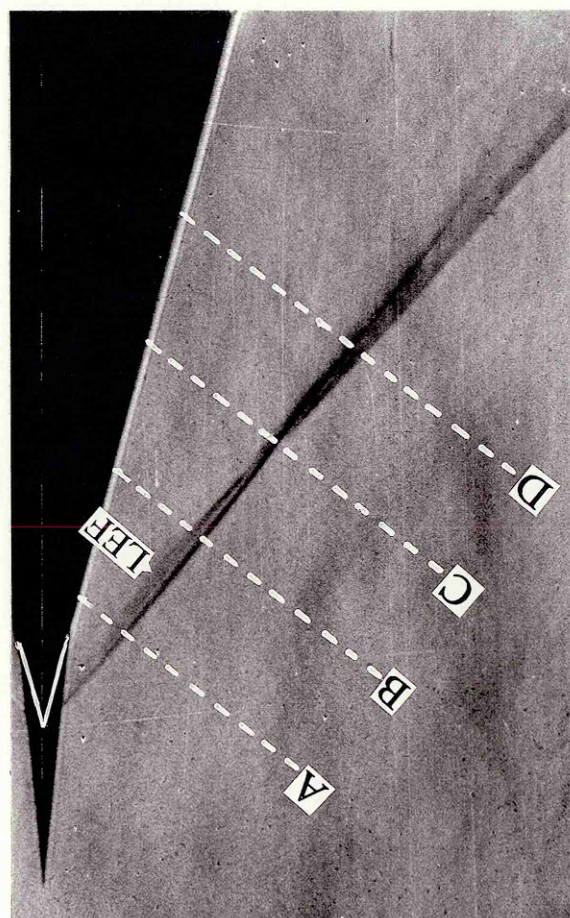
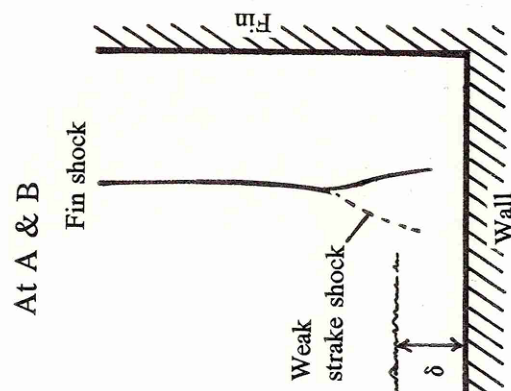


Fig.4-2 Continued (c) Strake 1

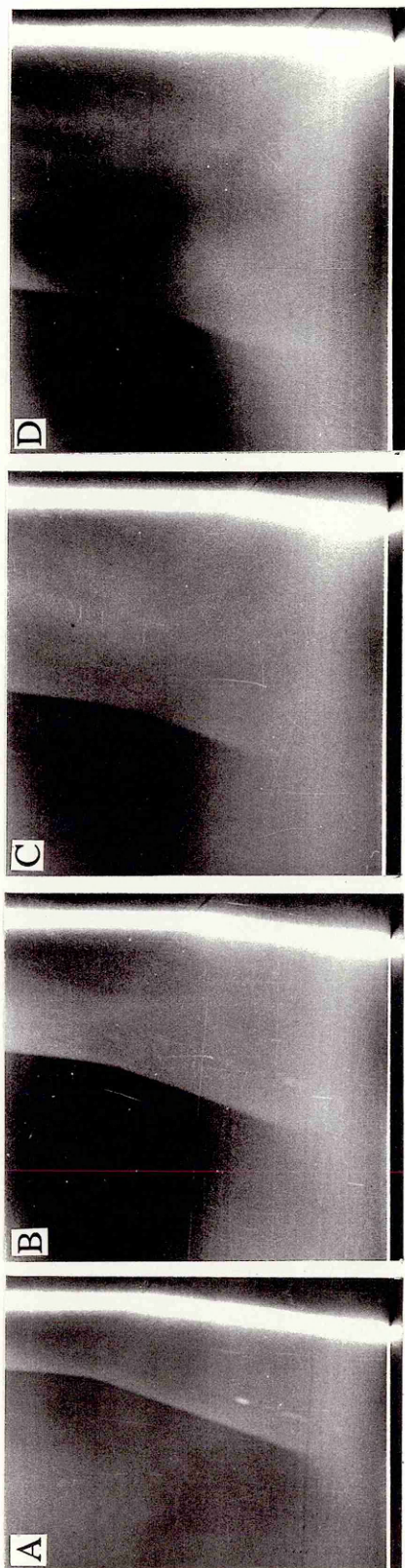
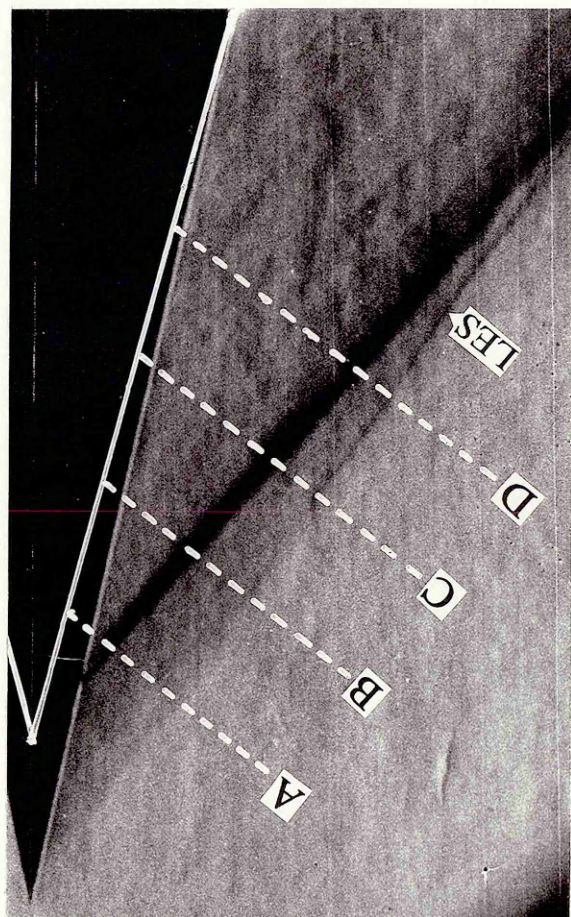
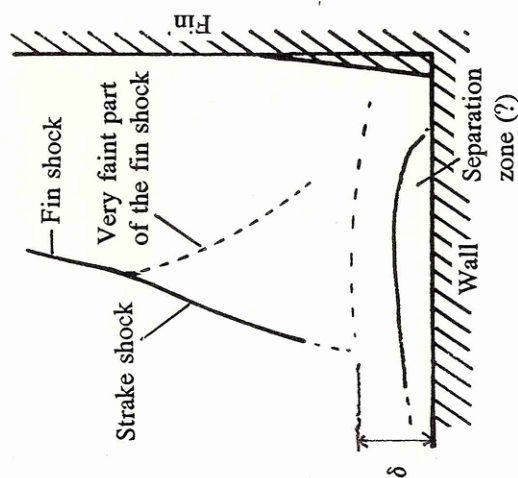


Fig.4-2 Continued (d) Strake 2

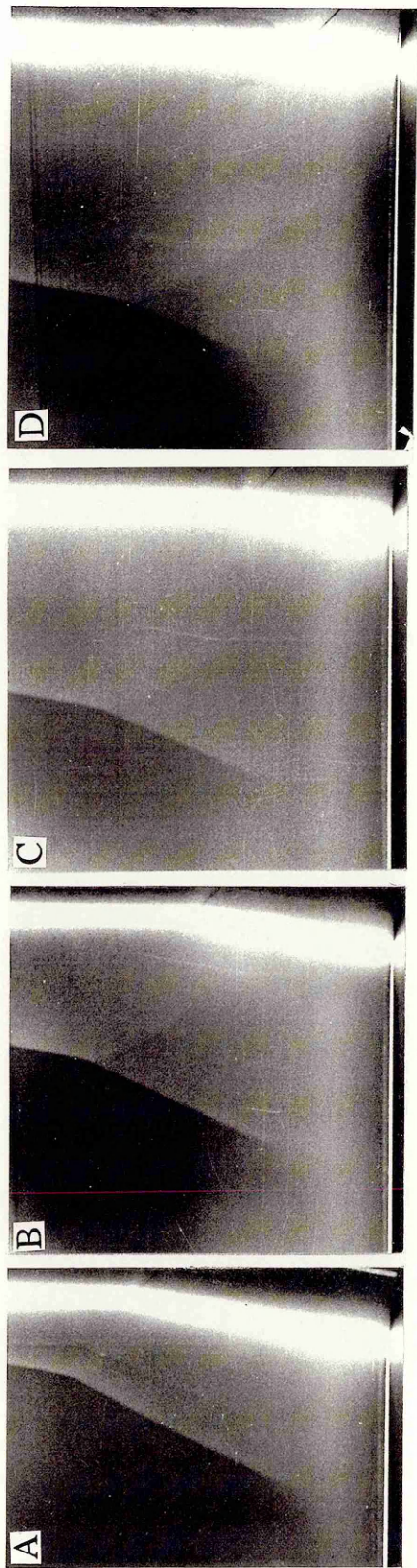
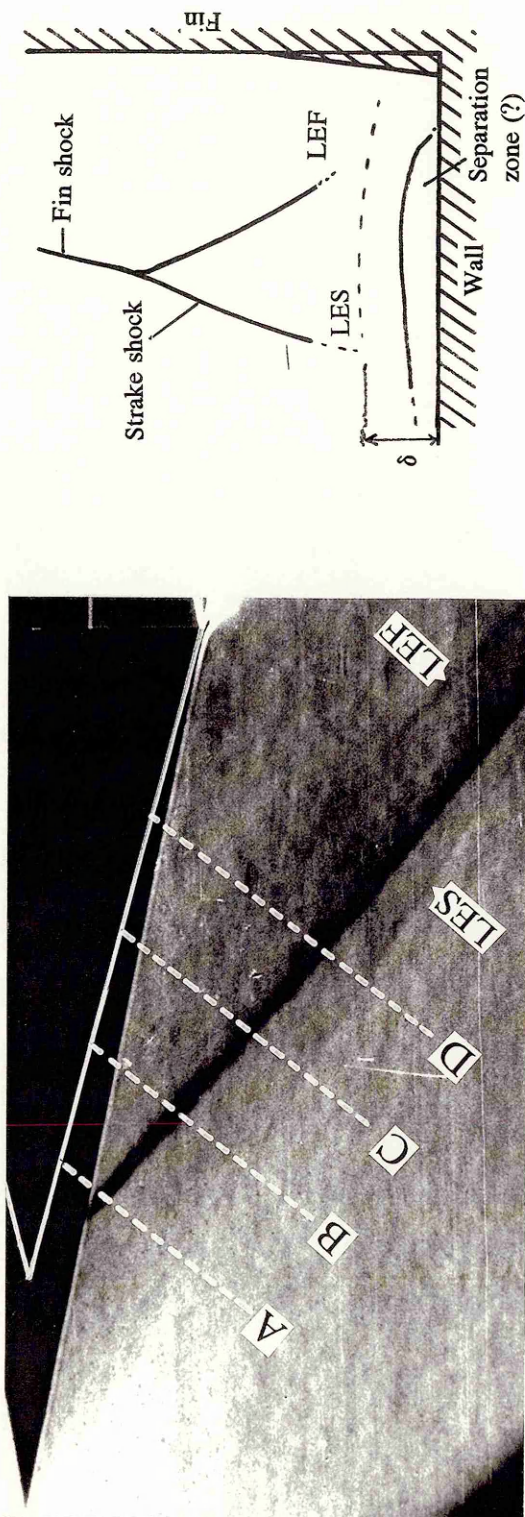


Fig.4-2 Continued (e) Strake 3

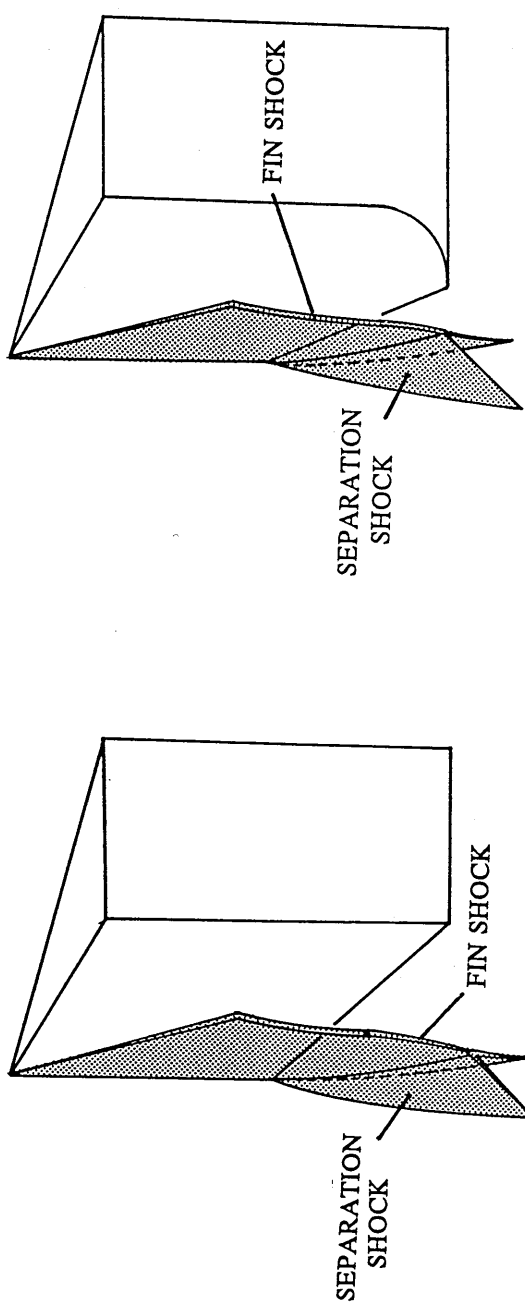
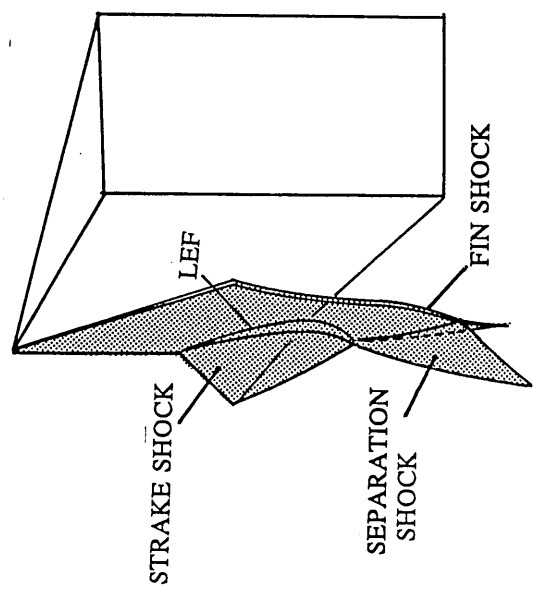
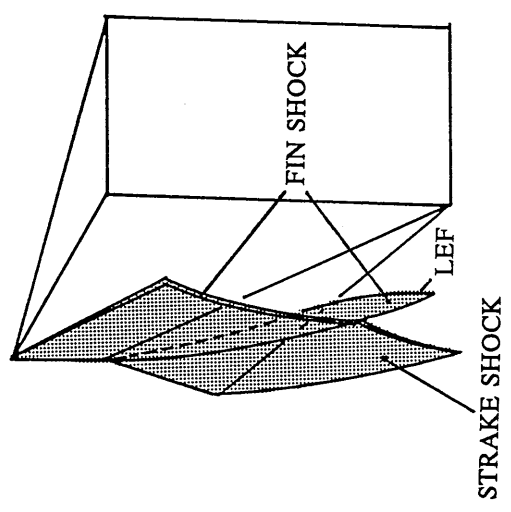


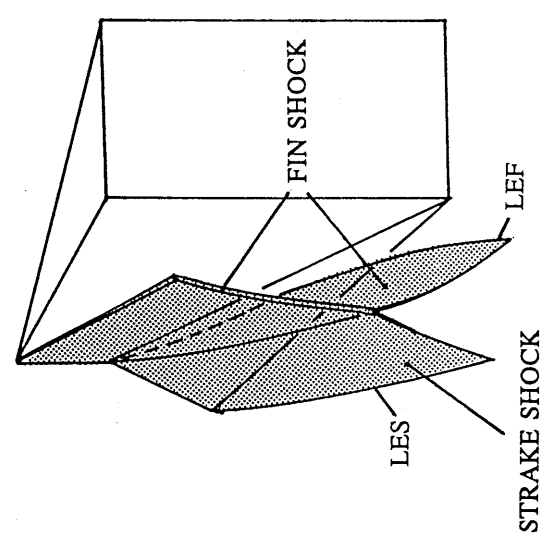
Fig.4-3 Three dimensional shock structures deduced from the LFV images



c) STRAKE 1

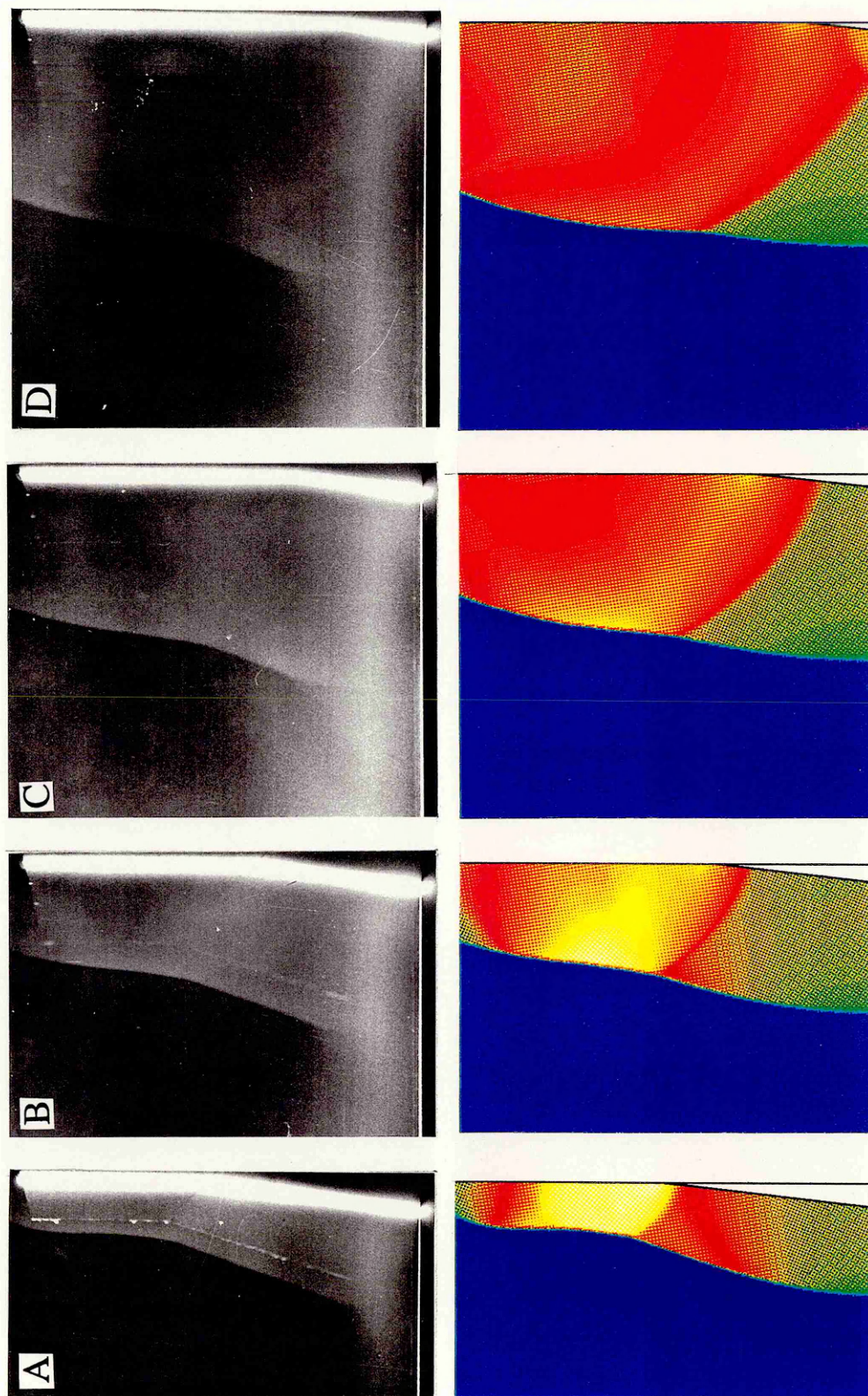


d) STRAKE 2



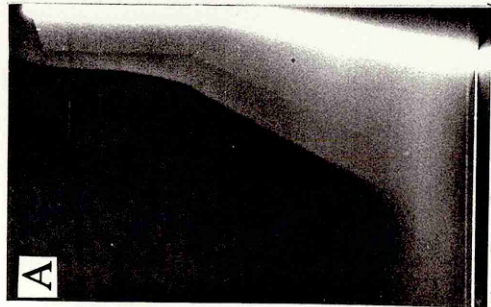
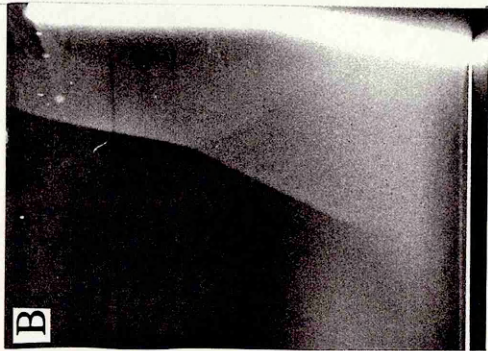
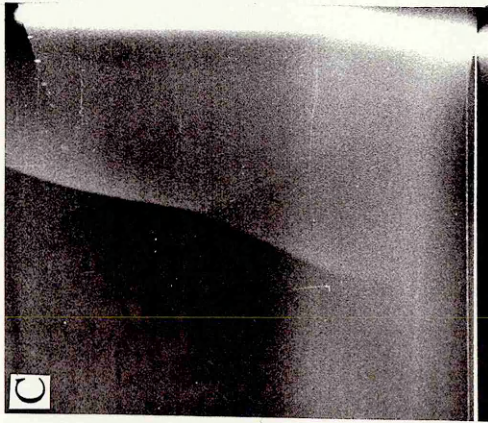
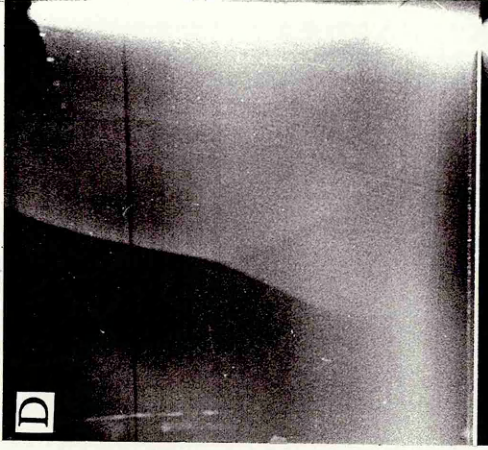
e) STRAKE 3

Fig.4-3 Continued

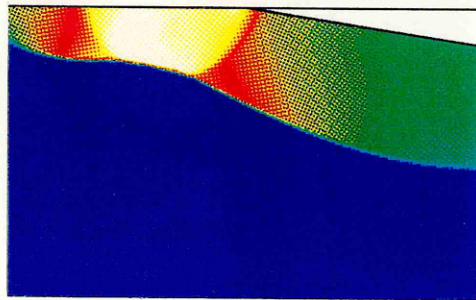
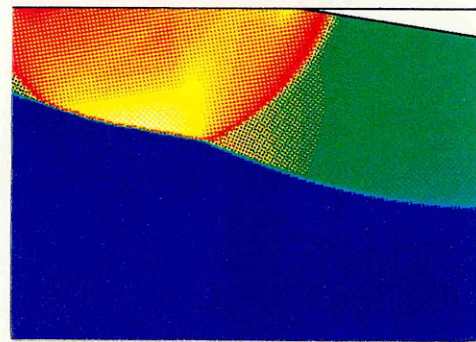
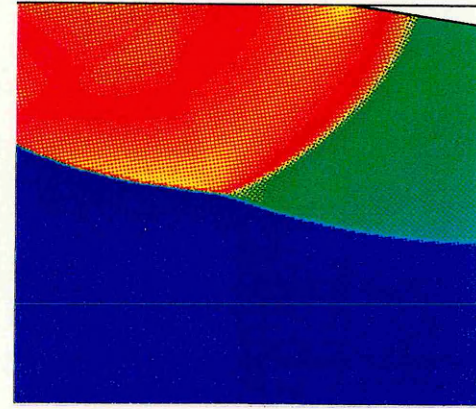
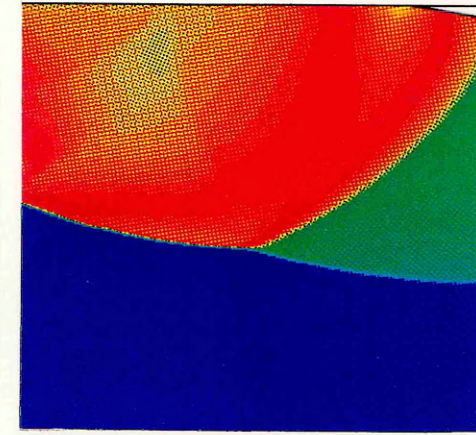


(a) Strake 2

Fig.4-4 LFV images and corresponding computed density contours ($M_{\infty}=2.06$)



LFV



CFD

(b) Strake 3

Fig.4-4 Continued

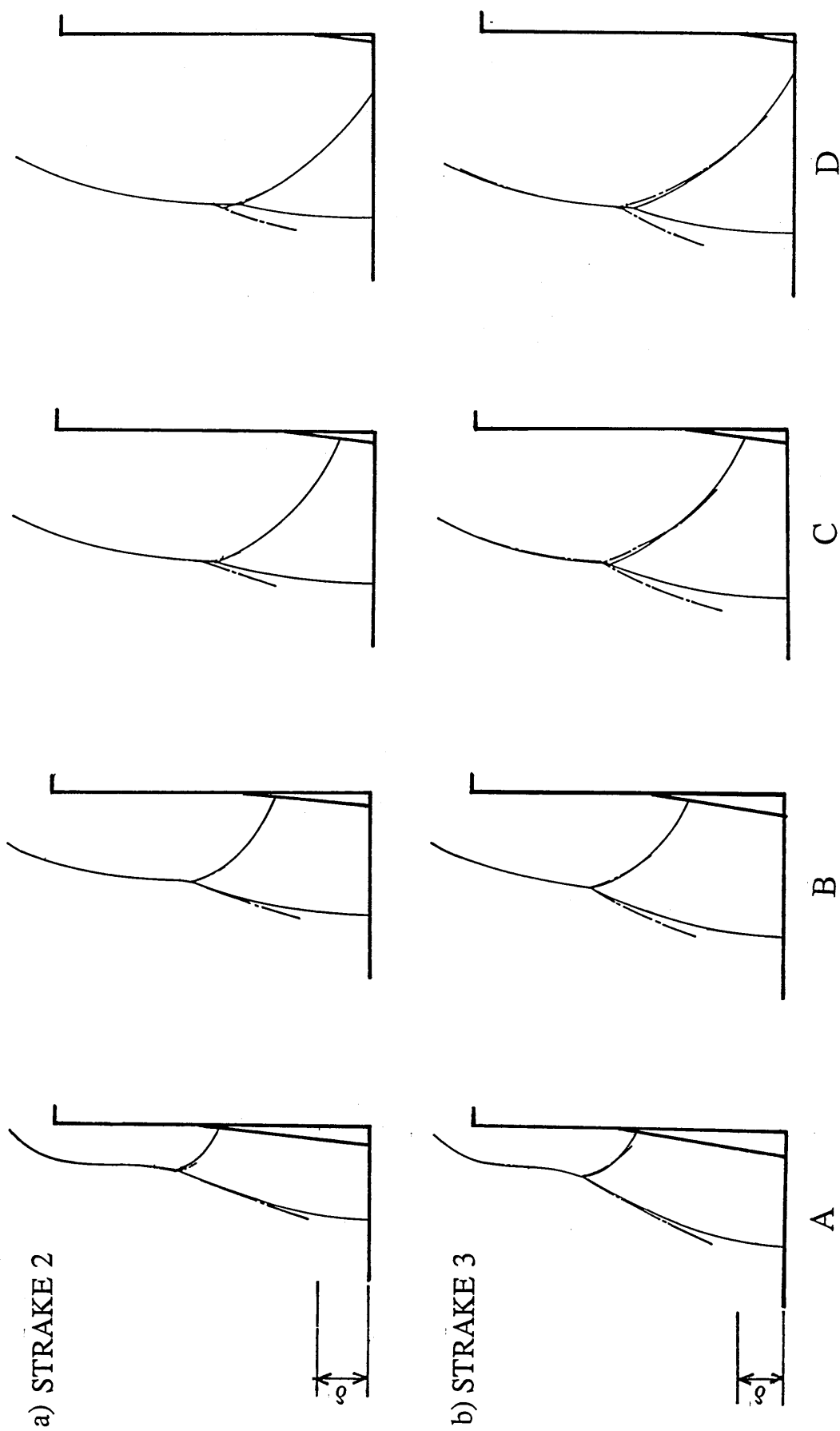


Fig.4-5 Comparison of shock shapes experimentally and numerically obtained ($M_{\infty}=2.06$)

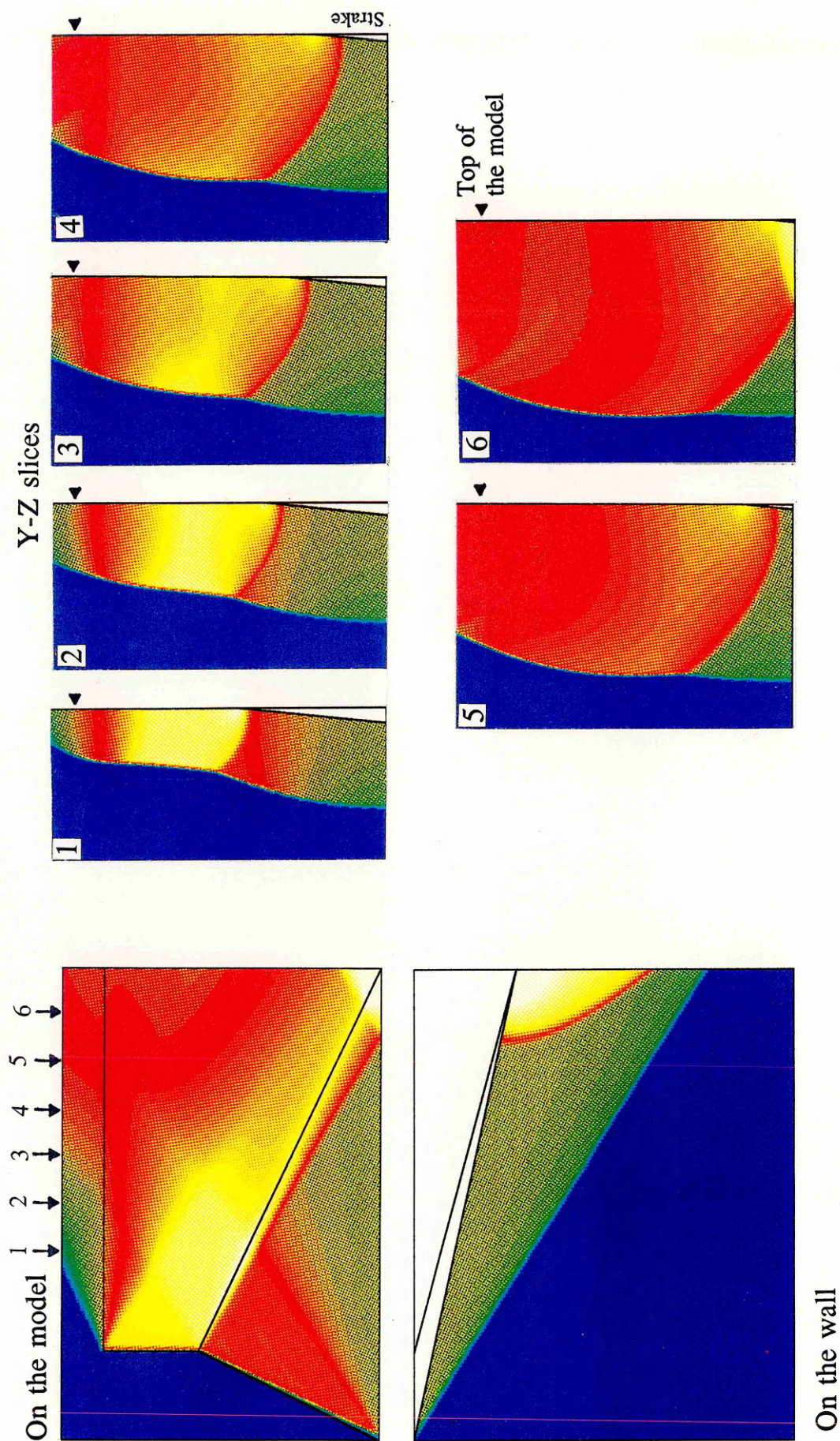


Fig.4-6 Computed inviscid flowfields ($M_\infty=2.46$) (a) Strake 2

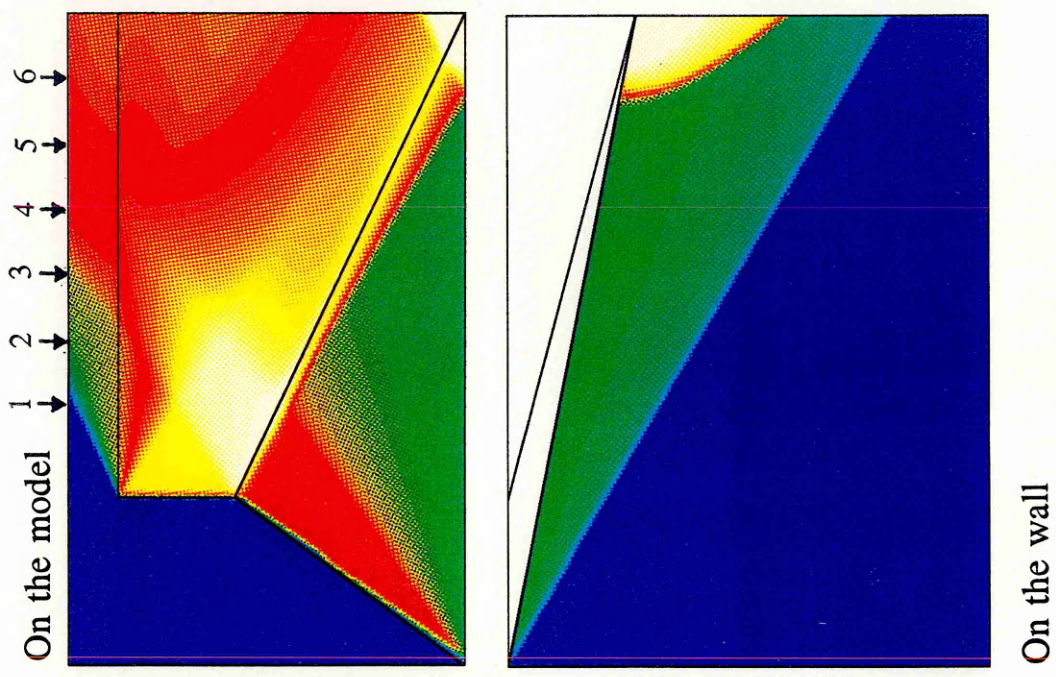
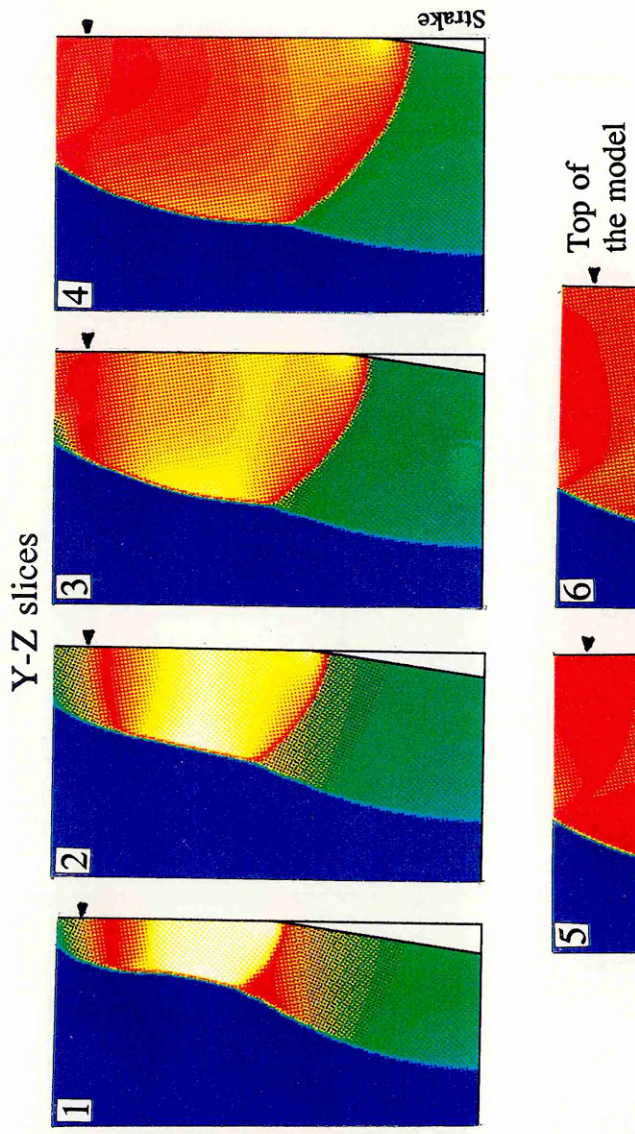
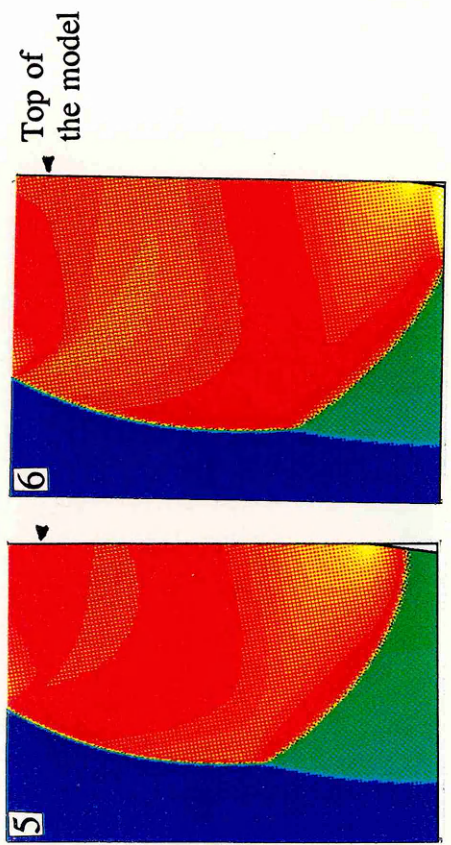
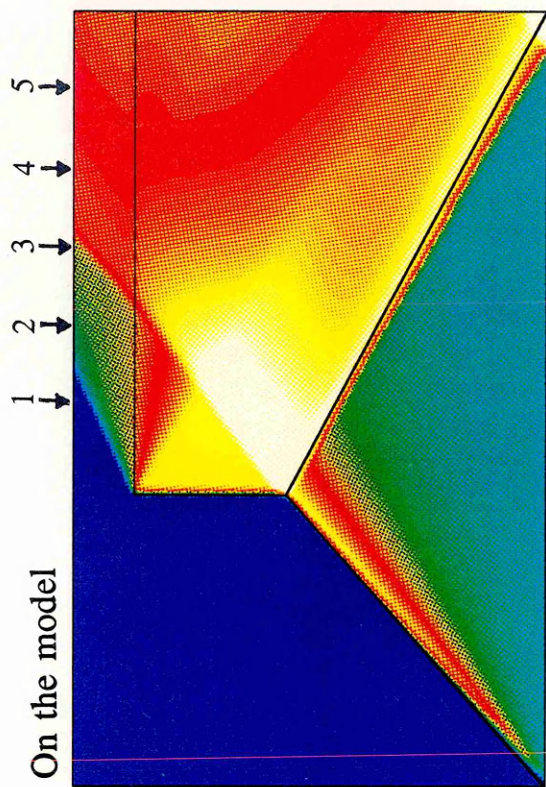
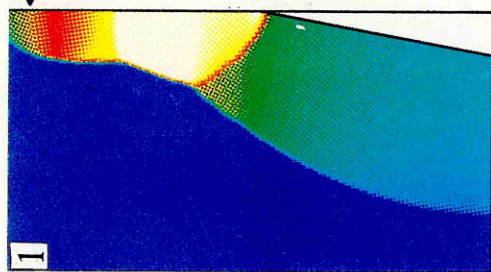
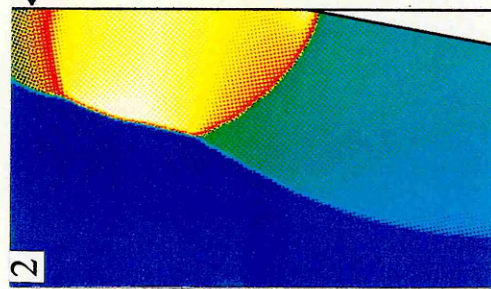
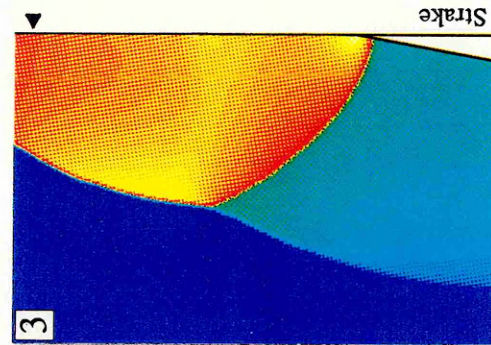
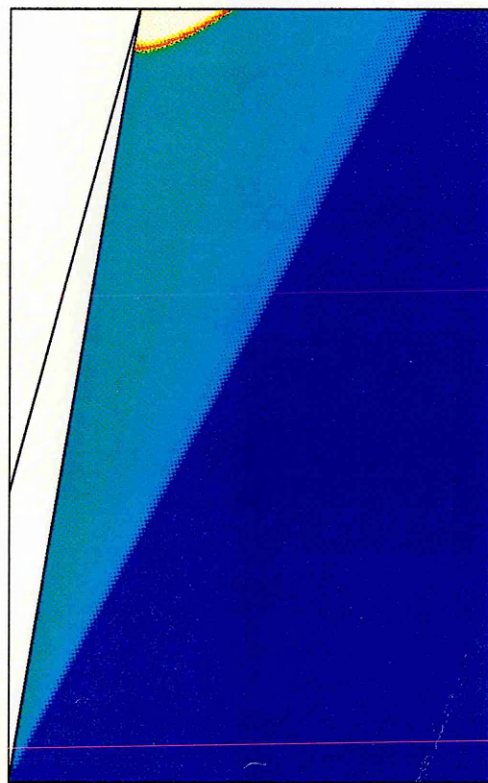
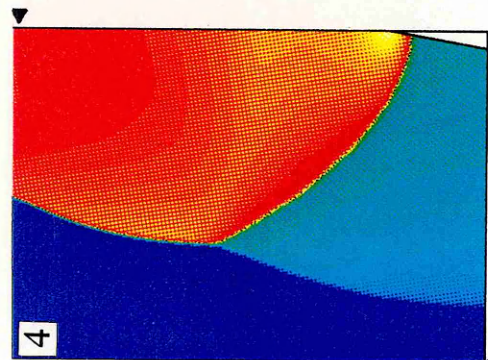
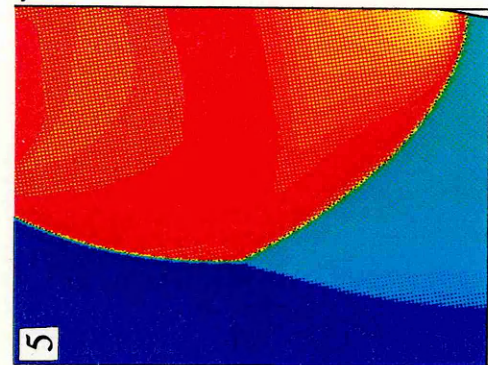


Fig.4-6 Continued (b) Strake 3

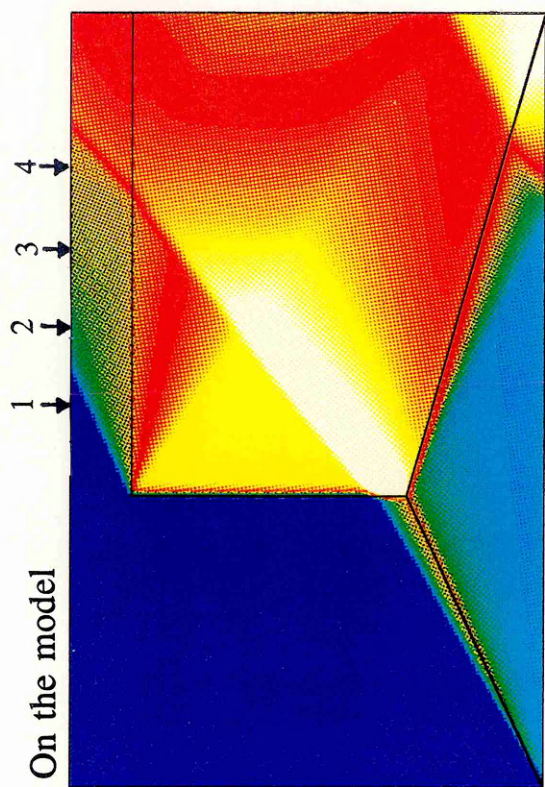


Y-Z slices



Top of
the model

Fig.4-6 Continued (c) Strake B1



Y-Z slices

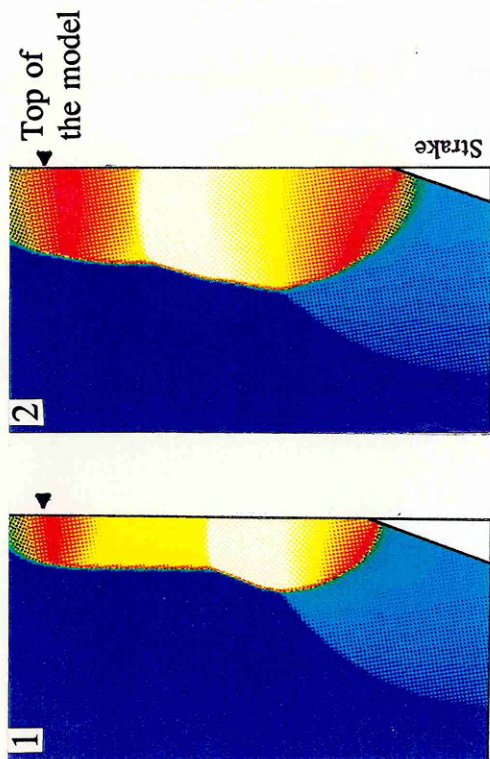
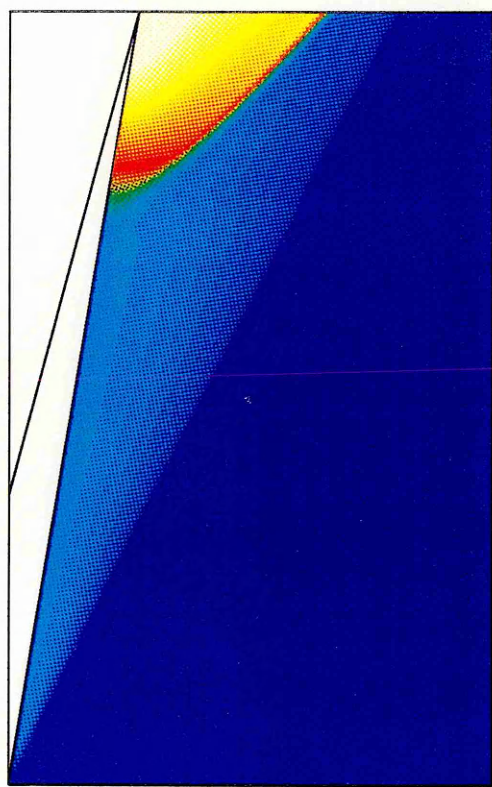
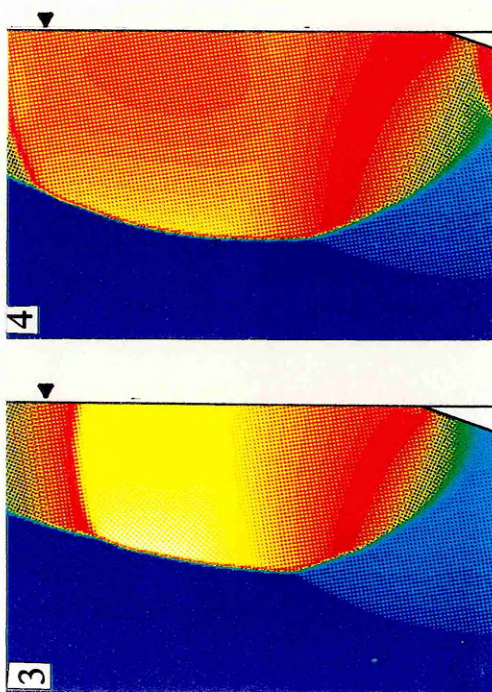
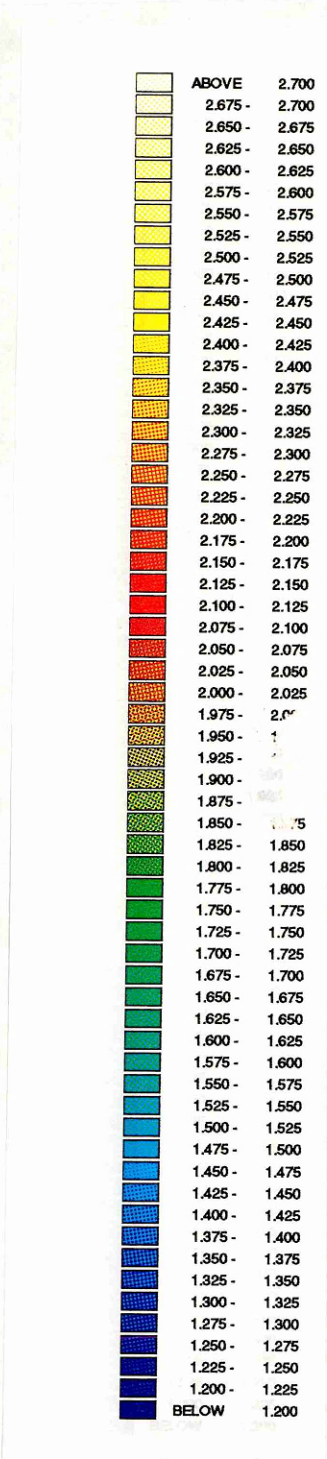
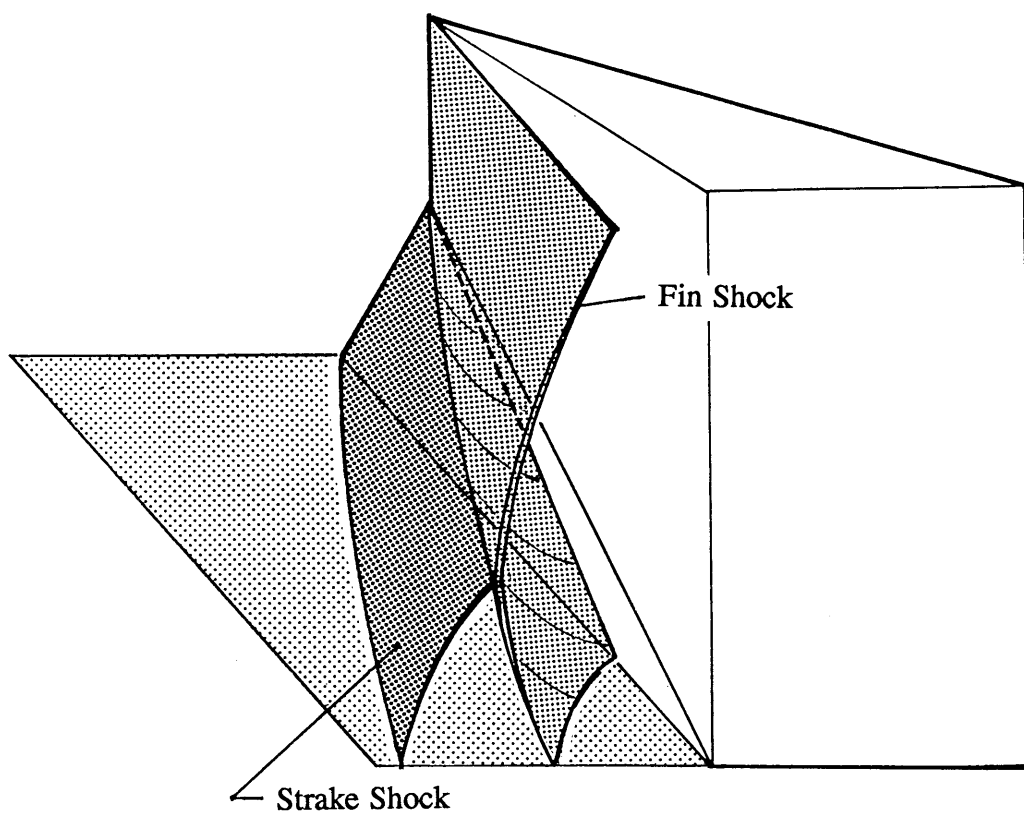


Fig.4-6 Continued (d) Strake B2



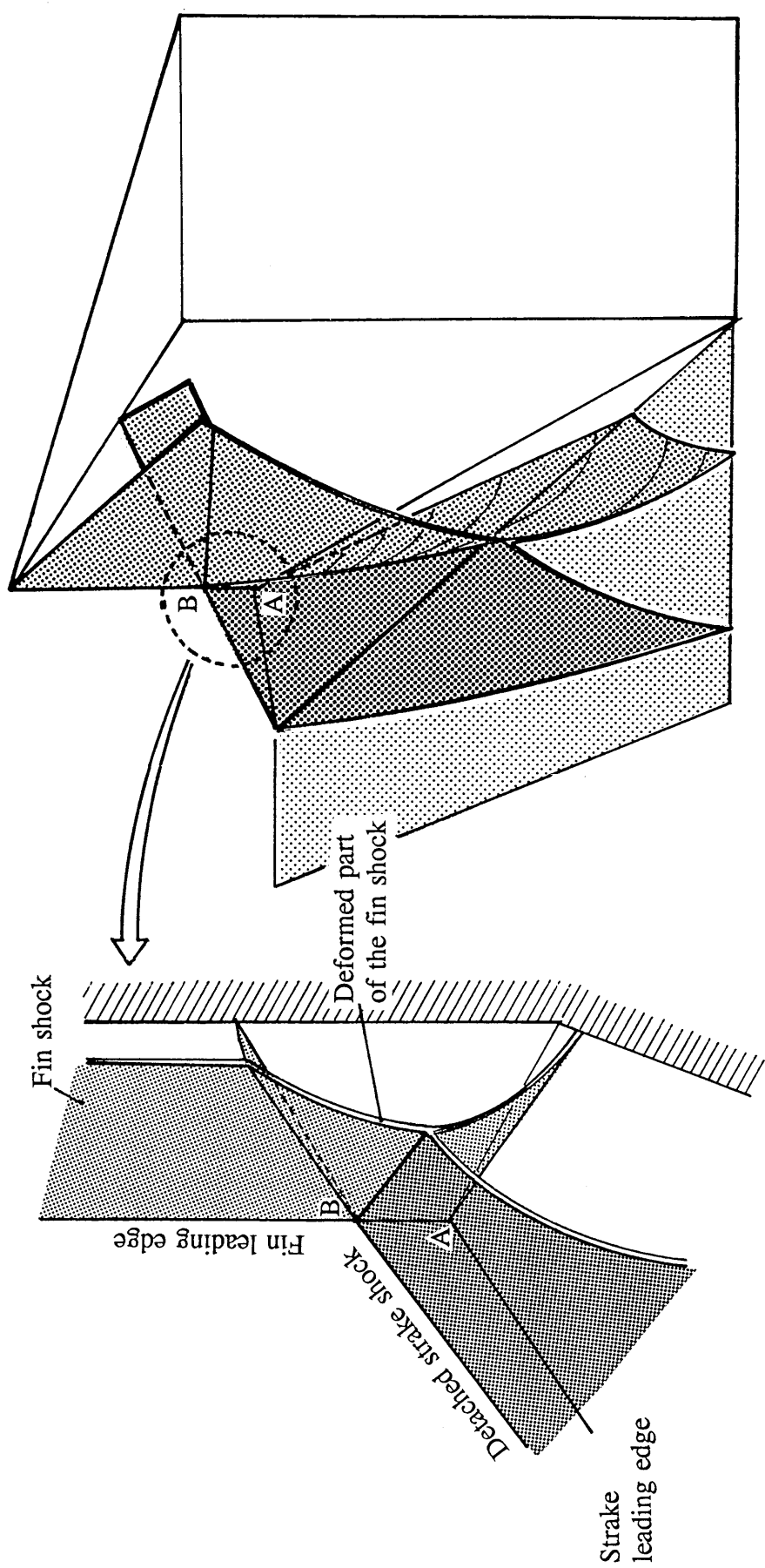
(e) Pressure levels

Fig.4-6 Continued



(a) With attached strake shock

Fig.4-7 Inviscid shock structures deduced from Fig.4-6



(b) With detached strake shock

Fig.4-7 Continued

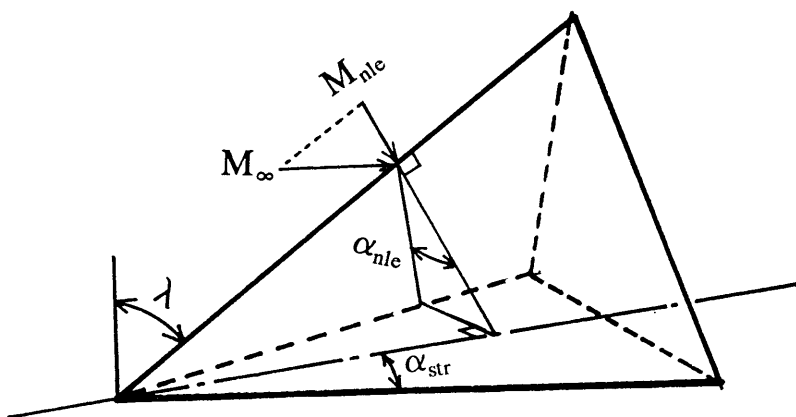
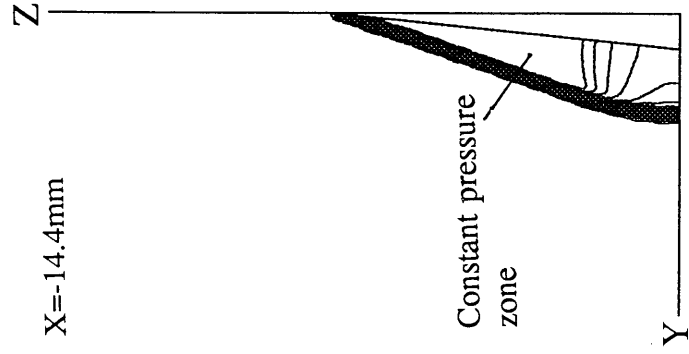
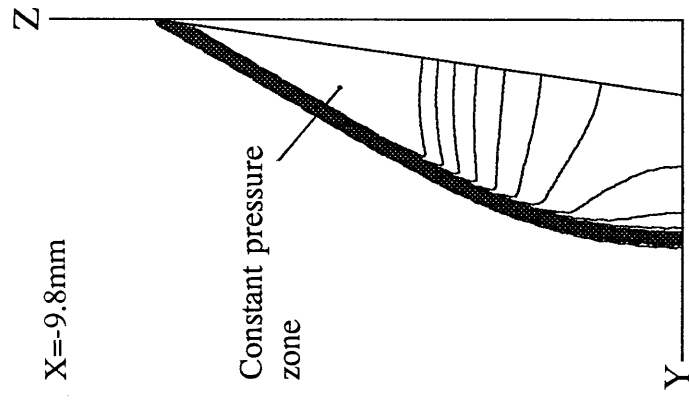


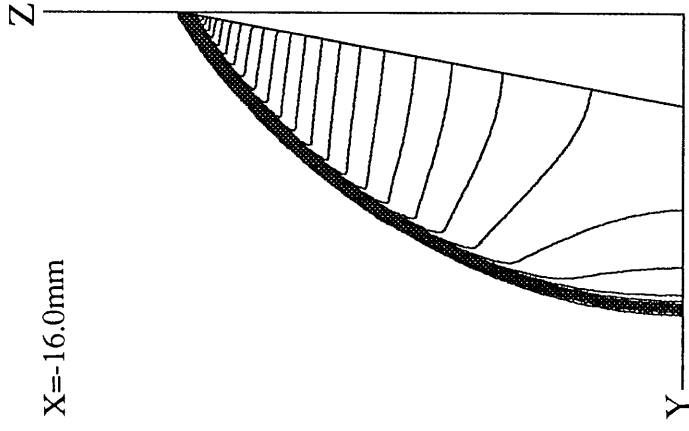
Fig.4-8 Definitions of α_{nle} and M_{nle}



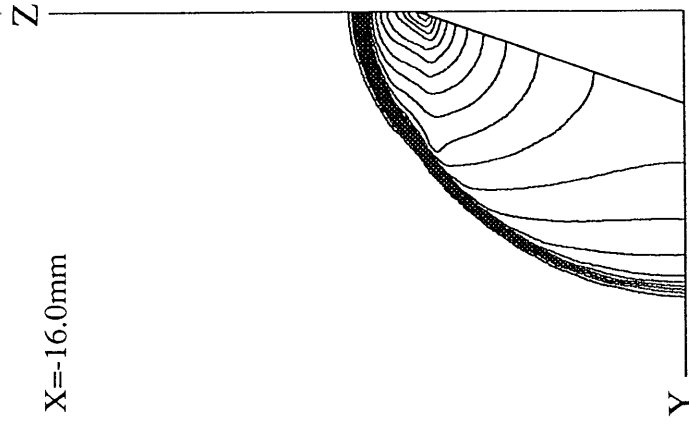
(a) Strake 2



(b) Strake 3



(c) Strake B1



(d) Strake B2

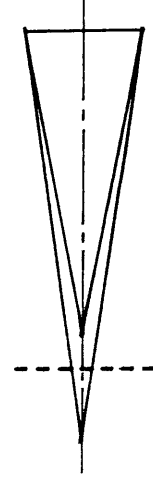
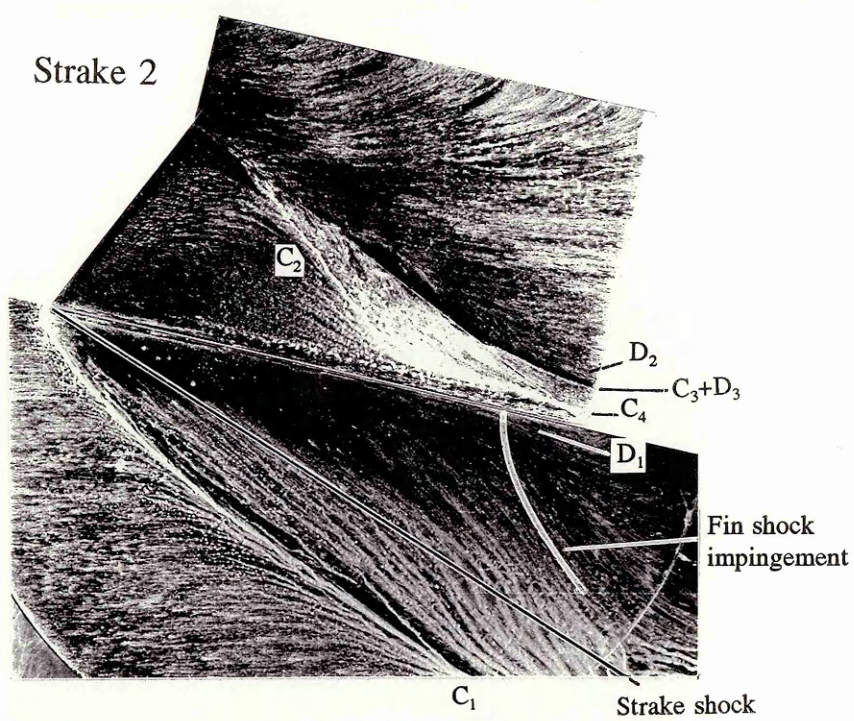


Fig.4-9 Isobar patterns upstream of the fin leading edge

(a) Strake 2



(b) Strake 3

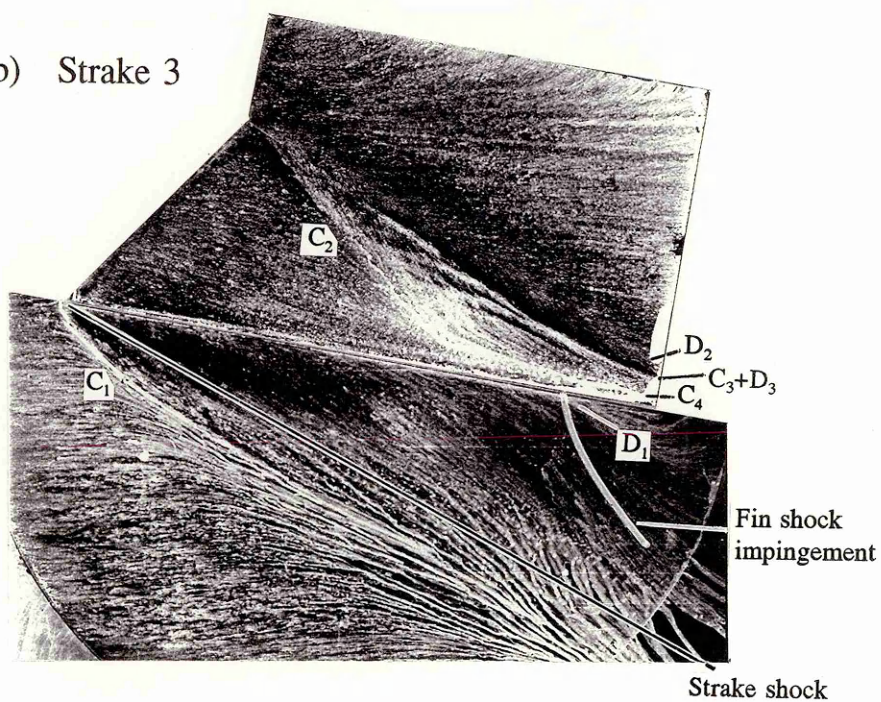


Fig.4-10 Oil flow patterns on the wall and model surface

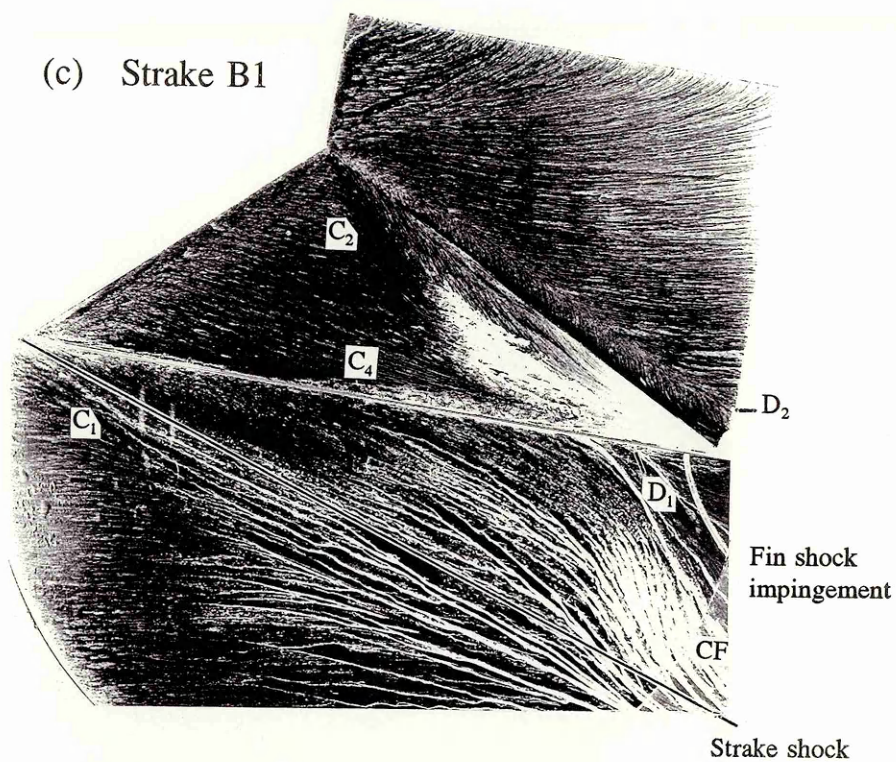
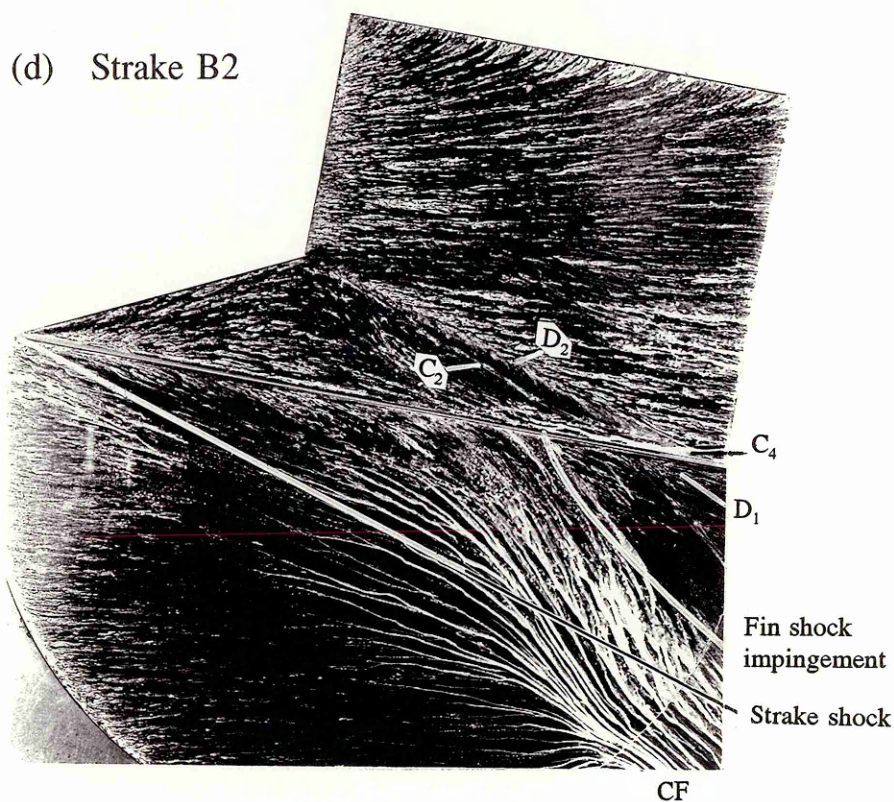


Fig.4-10 Continued

(e) Strake B3

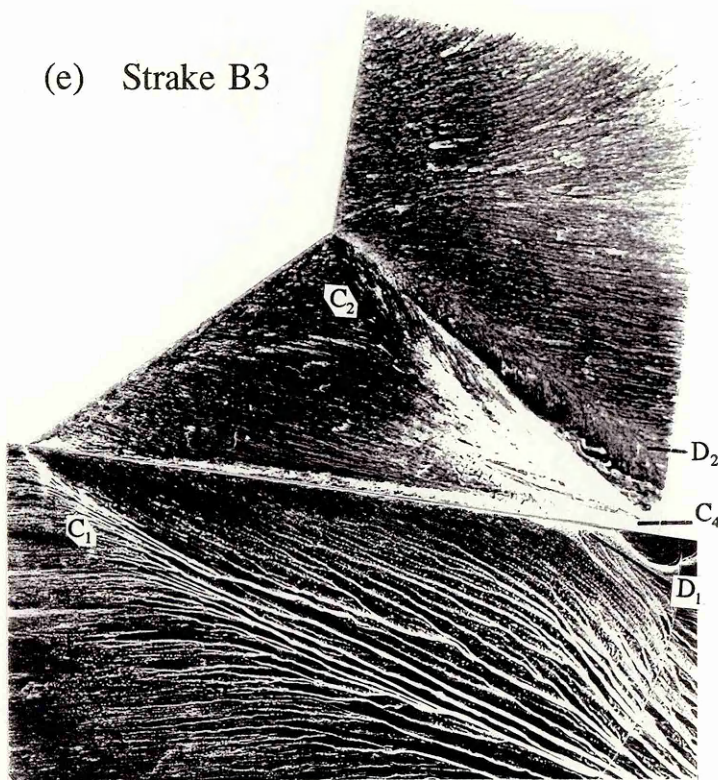


Fig.4-10 Continued

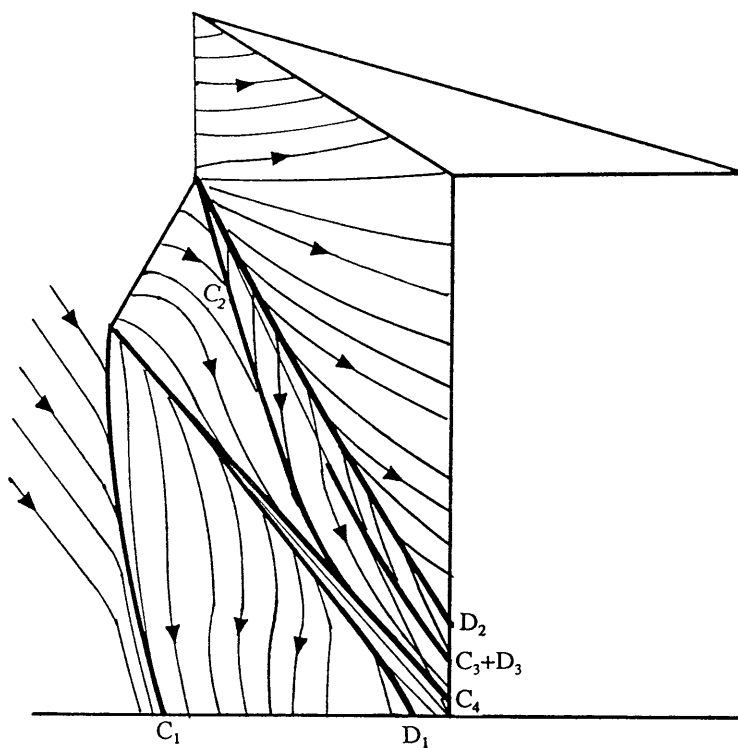
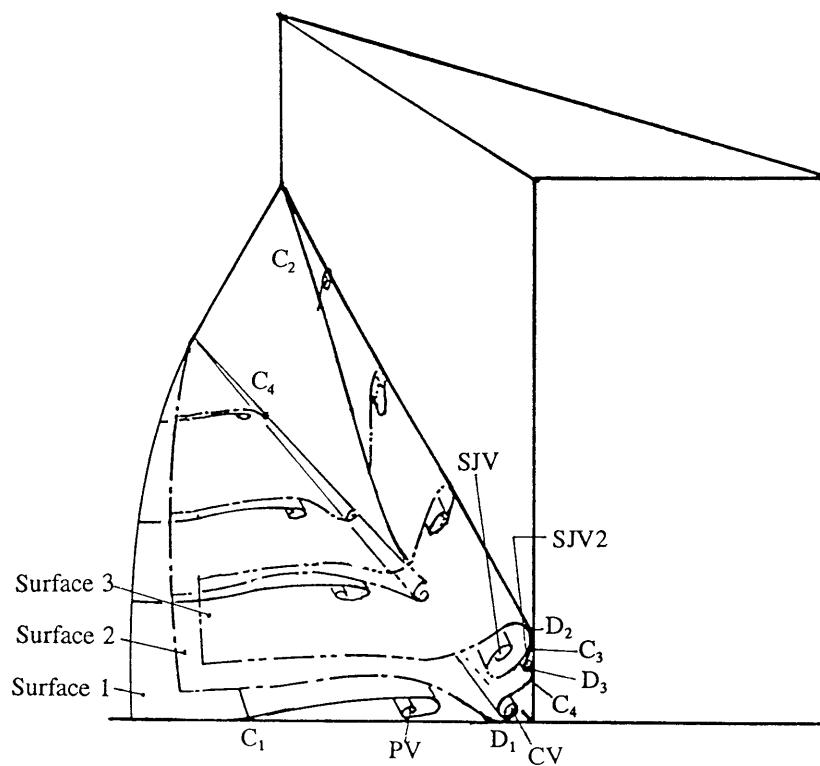
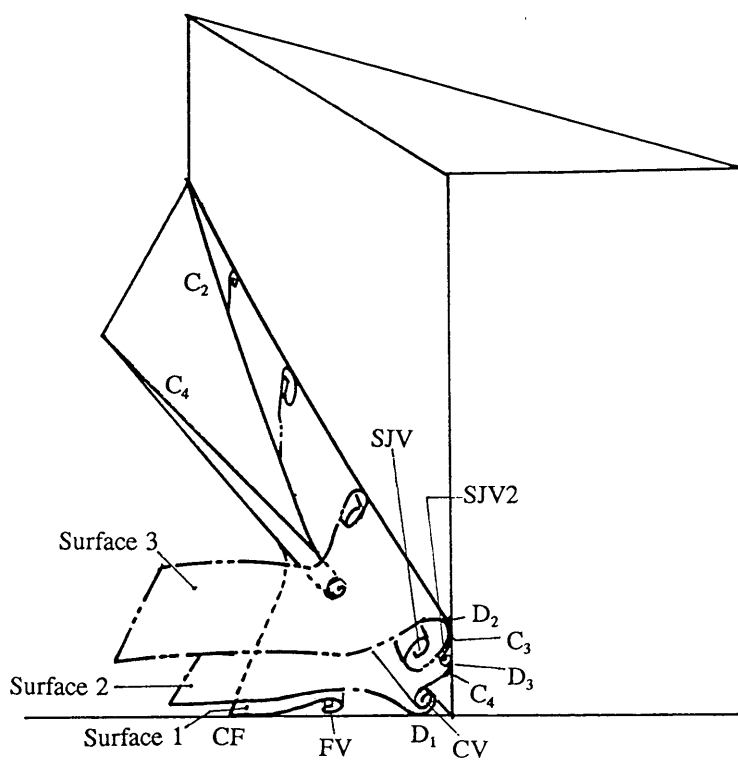


Fig.4-11 Surface streak patterns read from the oil flow visualizations



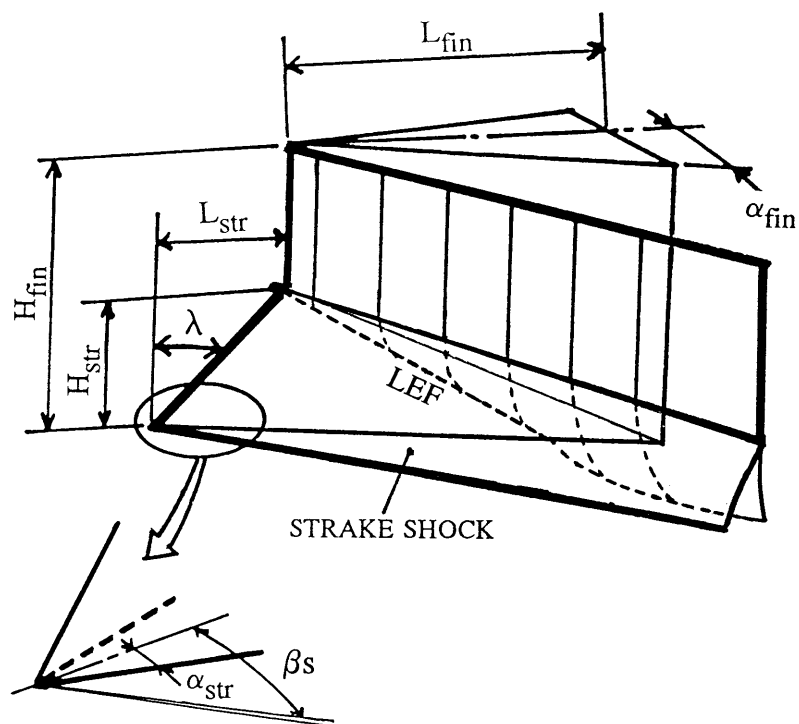
(a) With strong strake shock

Fig.4-12 Vortical flow structures

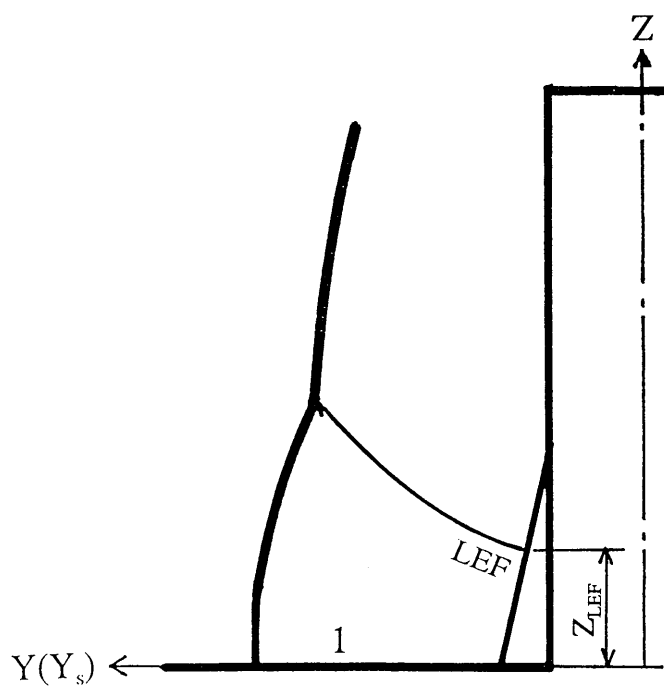


(b) With separation caused by the fin-shock impingement

Fig.4-12 Continued



(a) Three-dimensional schematic view



(b) On $Y(Y_s)$ - Z slice

Fig.5-1 Geometrical parameters of the model and the shock system

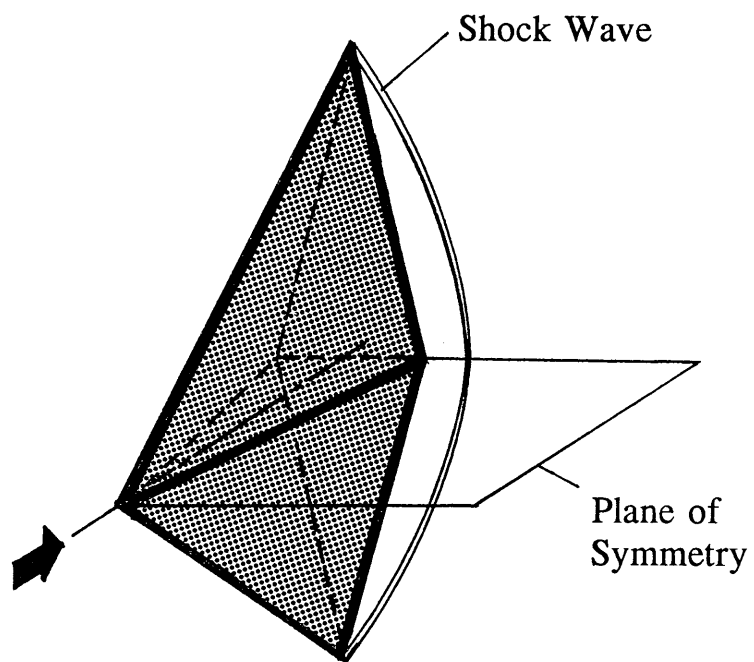
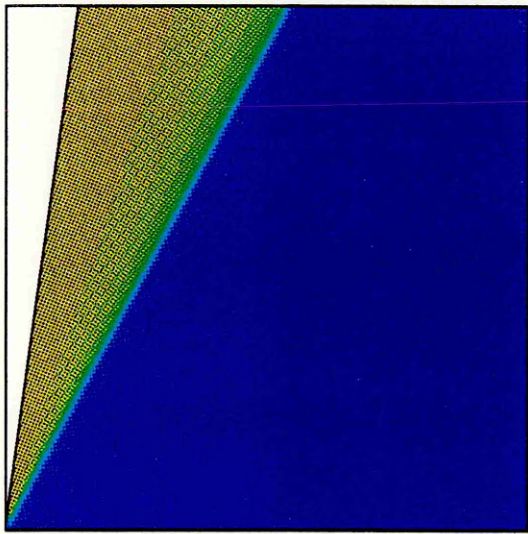


Fig.5-2 Schematic view of a rhombic delta wing and its inviscid shock wave

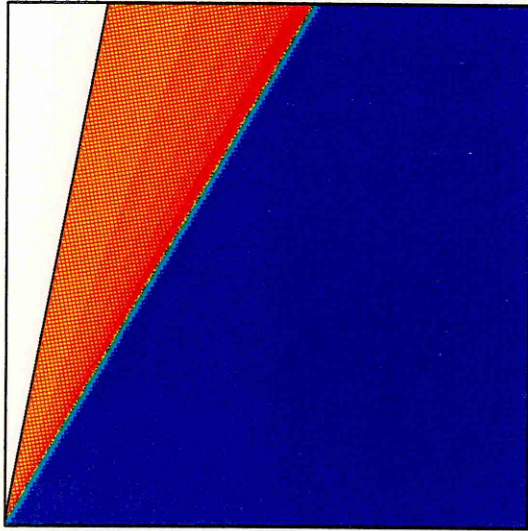
$$\lambda = 30^\circ$$

$$\alpha_{\text{str}} = 8^\circ$$



$$\lambda = 30^\circ$$

$$\alpha_{\text{str}} = 11^\circ$$



$$\lambda = 30^\circ$$

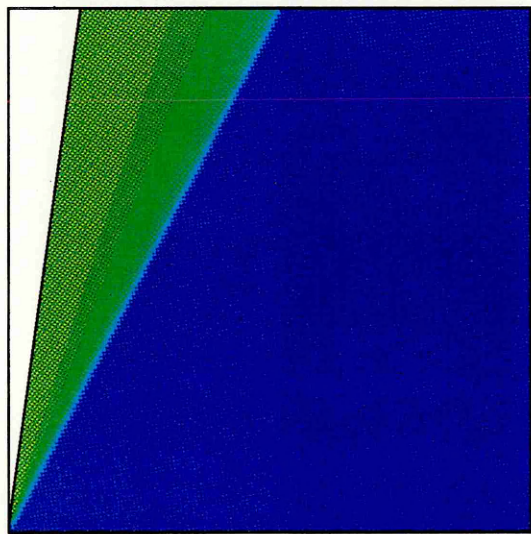
$$\alpha_{\text{str}} = 14^\circ$$



Fig.5-3 Static pressure contours numerically obtained in the plane of symmetry of the rhombic delta wing

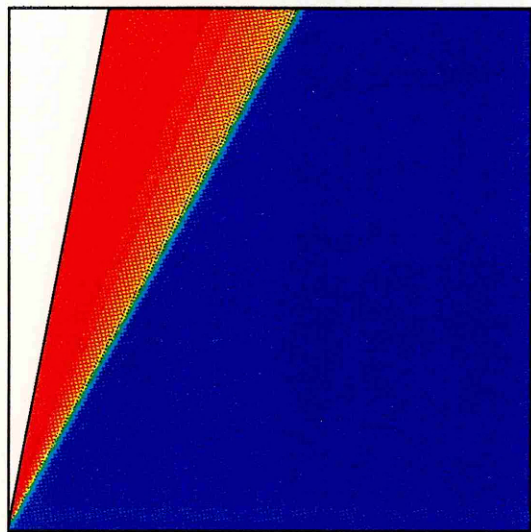
$$\lambda = 45^\circ$$

$$\alpha_{\text{str}} = 8^\circ$$



$$\lambda = 45^\circ$$

$$\alpha_{\text{str}} = 11^\circ$$



$$\lambda = 45^\circ$$

$$\alpha_{\text{str}} = 14^\circ$$

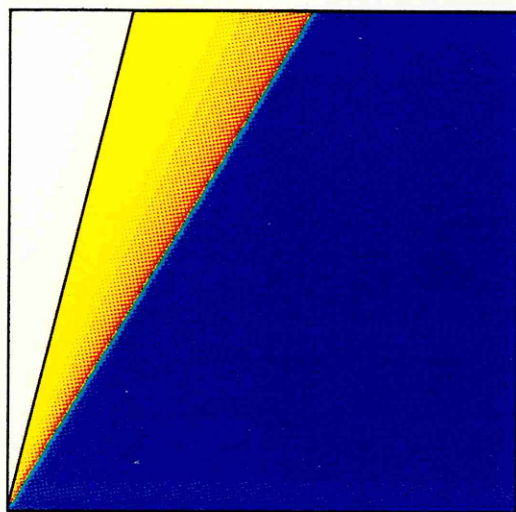
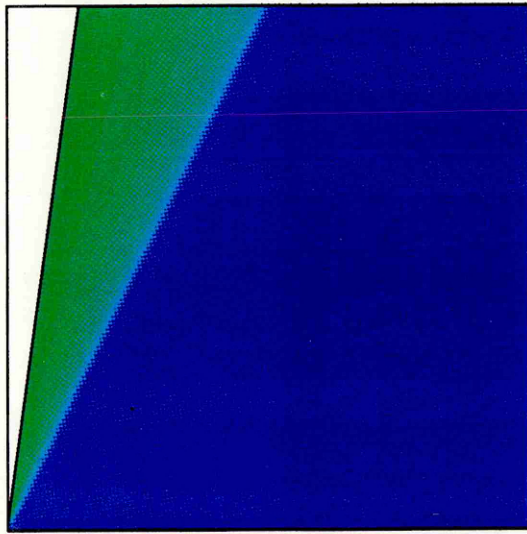


Fig.5-3 Continued

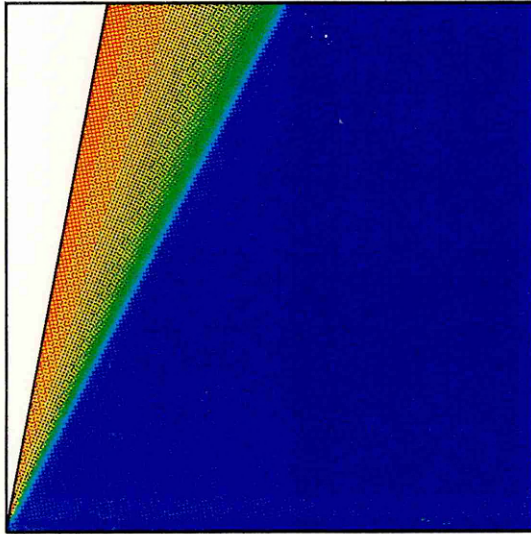
$$\lambda = 60^\circ$$

$$\alpha_{\text{str}} = 8^\circ$$



$$\lambda = 60^\circ$$

$$\alpha_{\text{str}} = 11^\circ$$



$$\lambda = 60^\circ$$

$$\alpha_{\text{str}} = 14^\circ$$

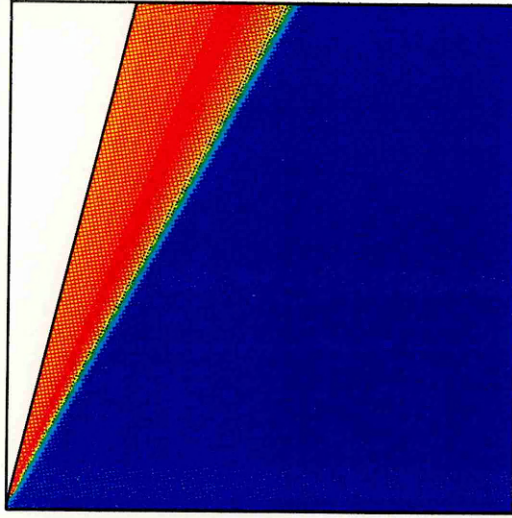


Fig.5-3 Continued

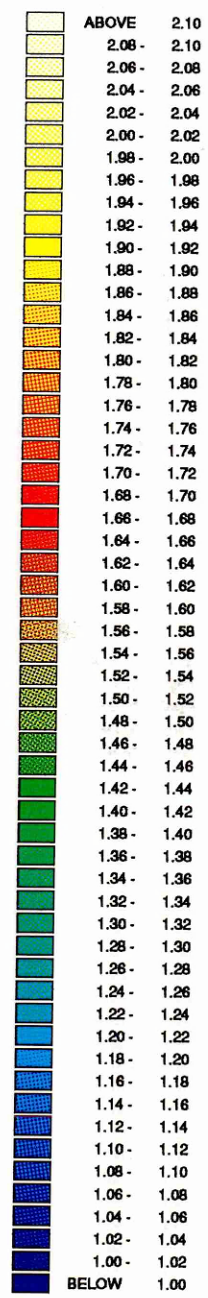


Fig.5-3 Continued

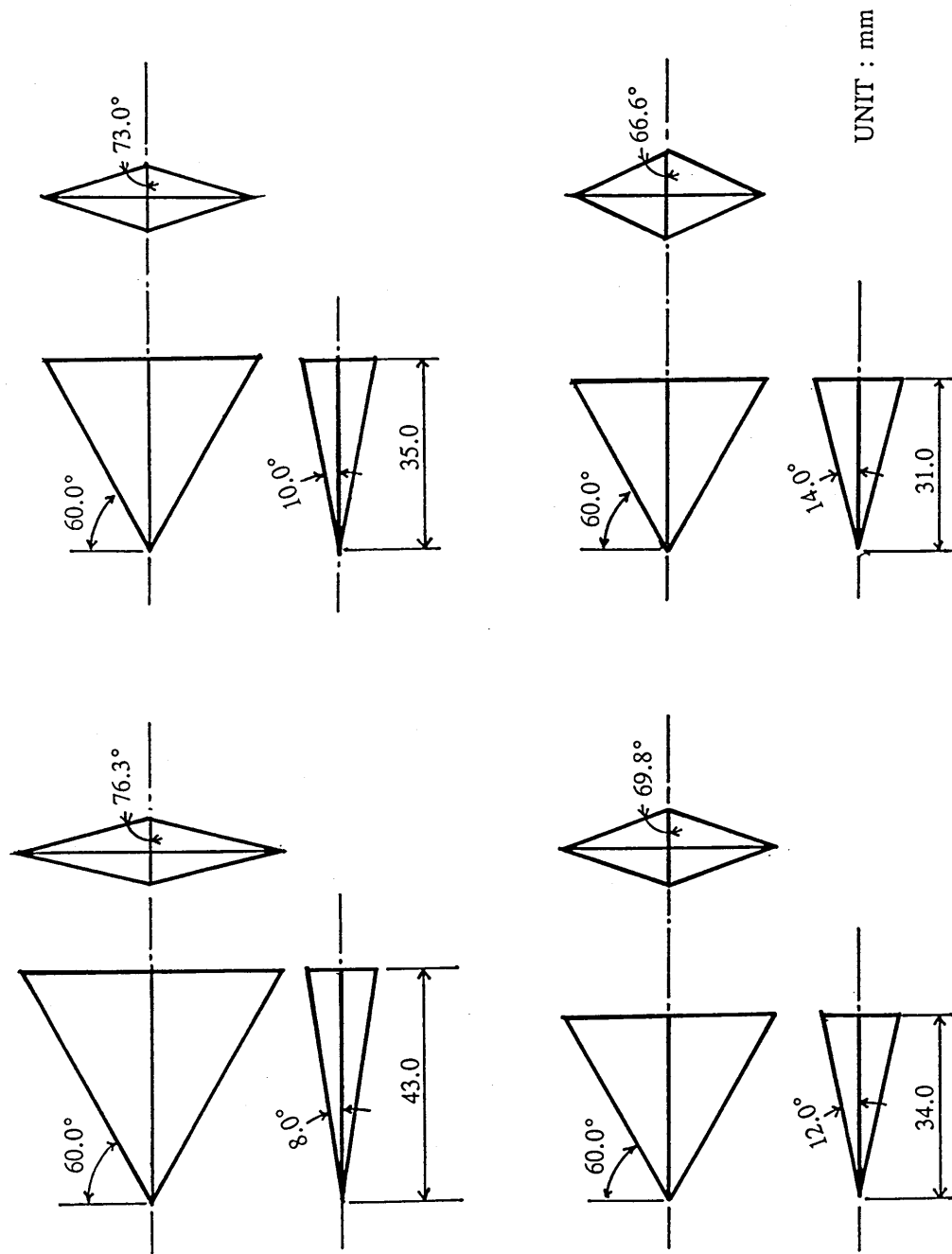
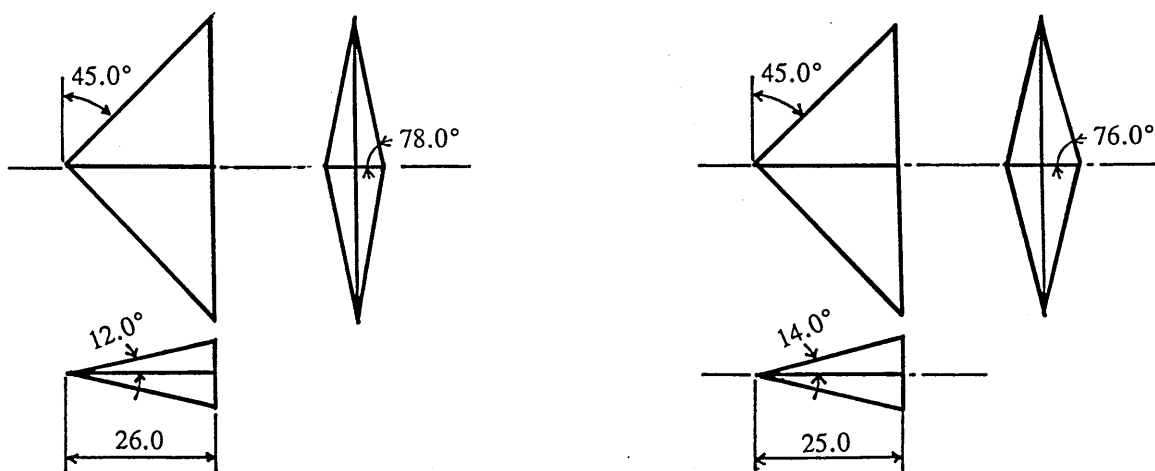
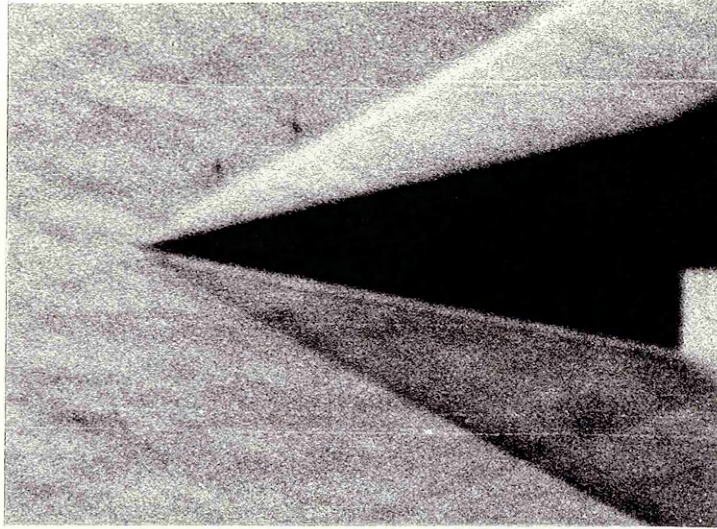


Fig.5-4 Rhombic delta wing models

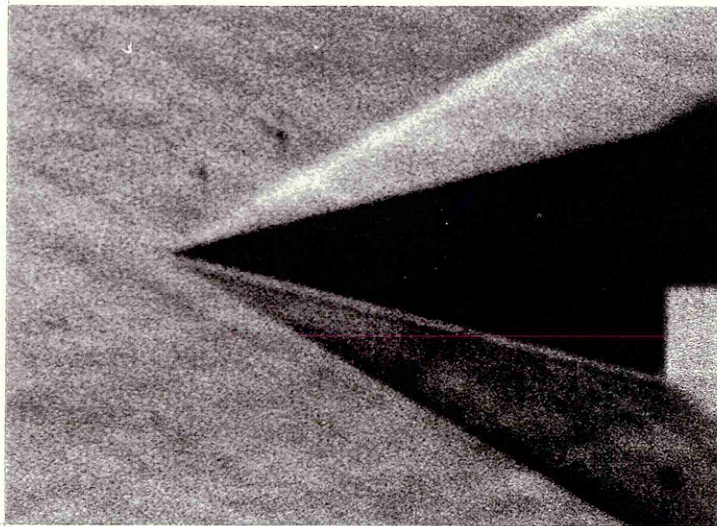


UNIT : mm

Fig.5-4 Continued

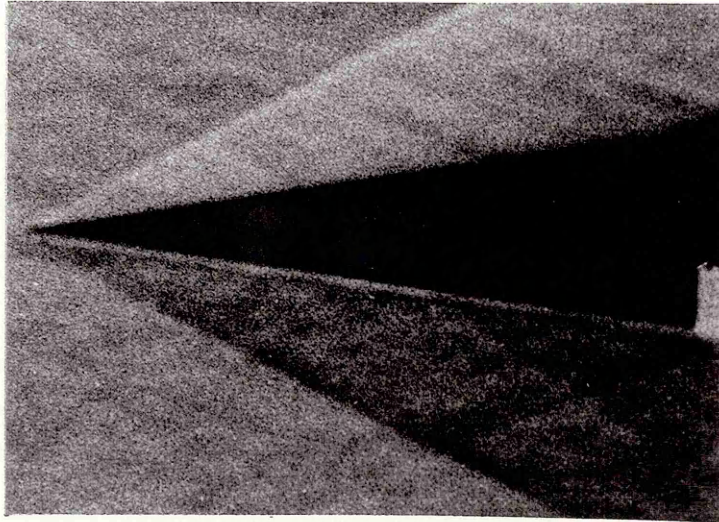


$$\lambda = 60^\circ \quad \alpha_{\text{str}} = 12^\circ$$

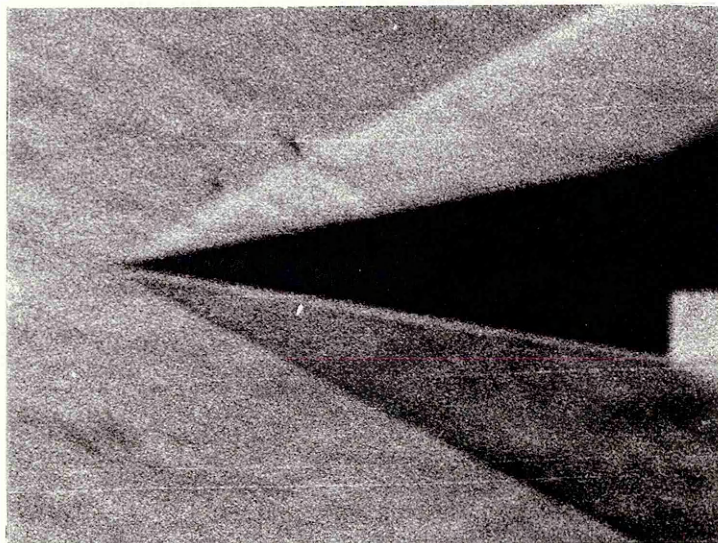


$$\lambda = 60^\circ \quad \alpha_{\text{str}} = 14^\circ$$

Fig.5-5 Schlieren images of the shock waves caused by several rhombic delta wings with various λ and α_{str}

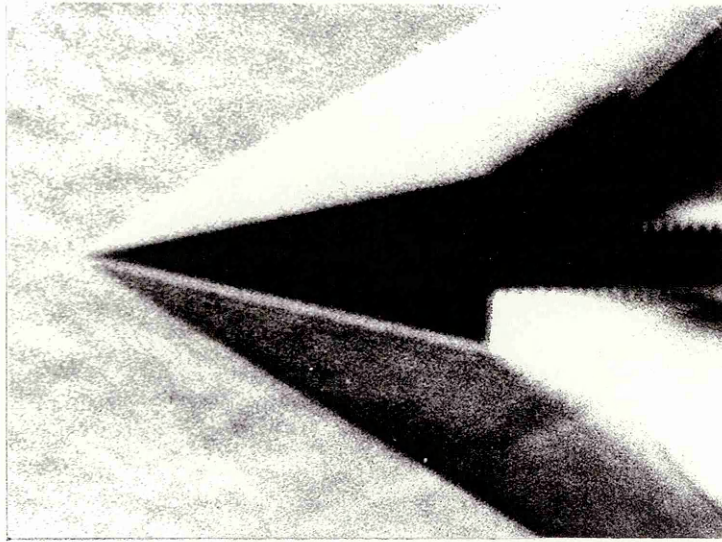


$$\lambda=60^{\circ} \quad \alpha_{\text{str}}=8^{\circ}$$

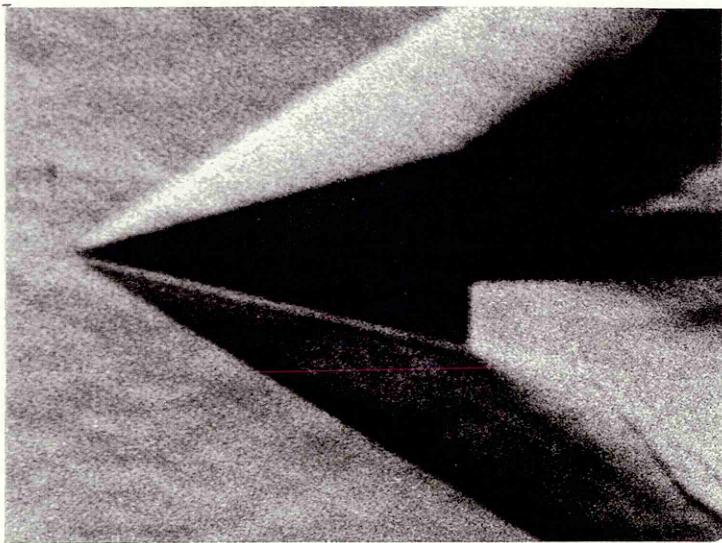


$$\lambda=60^{\circ} \quad \alpha_{\text{str}}=10^{\circ}$$

Fig.5-5 Continued



$$\lambda=45^{\circ} \quad \alpha_{\text{str}}=12^{\circ}$$



$$\lambda=45^{\circ} \quad \alpha_{\text{str}}=14^{\circ}$$

Fig.5-5 Continued

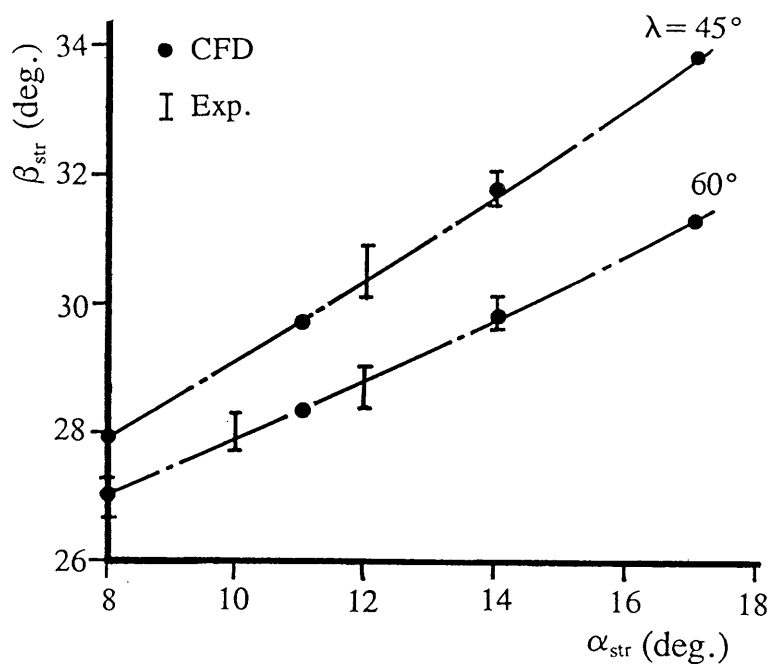


Fig.5-6 Comparison of experimental and CFD strake-shock angles

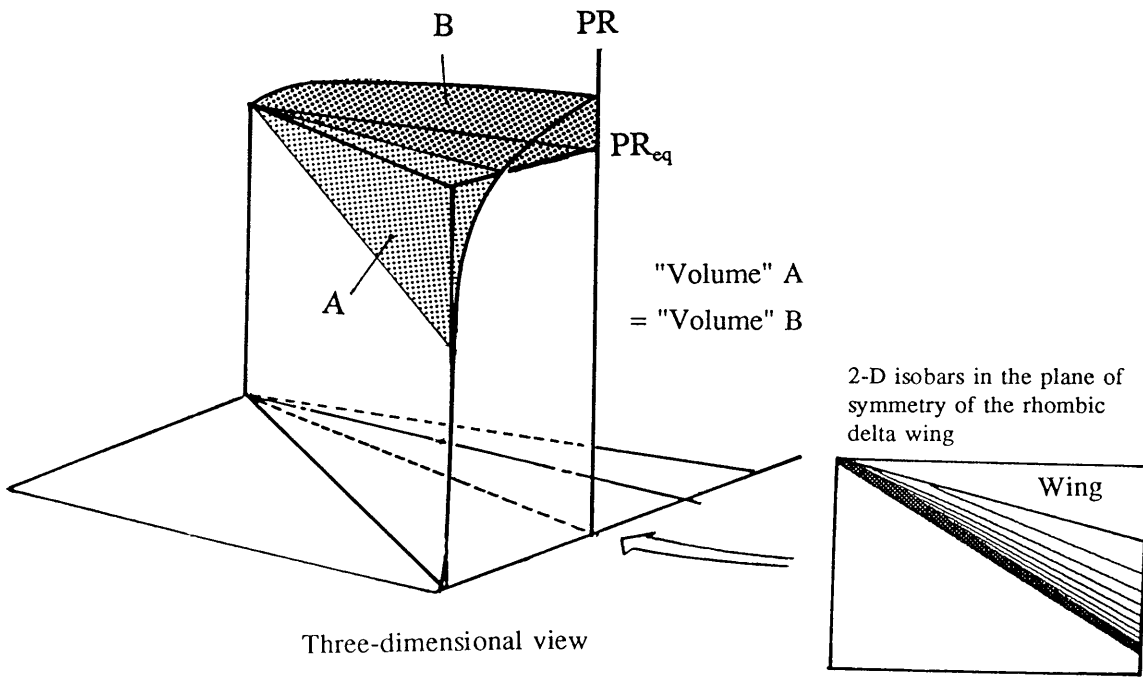
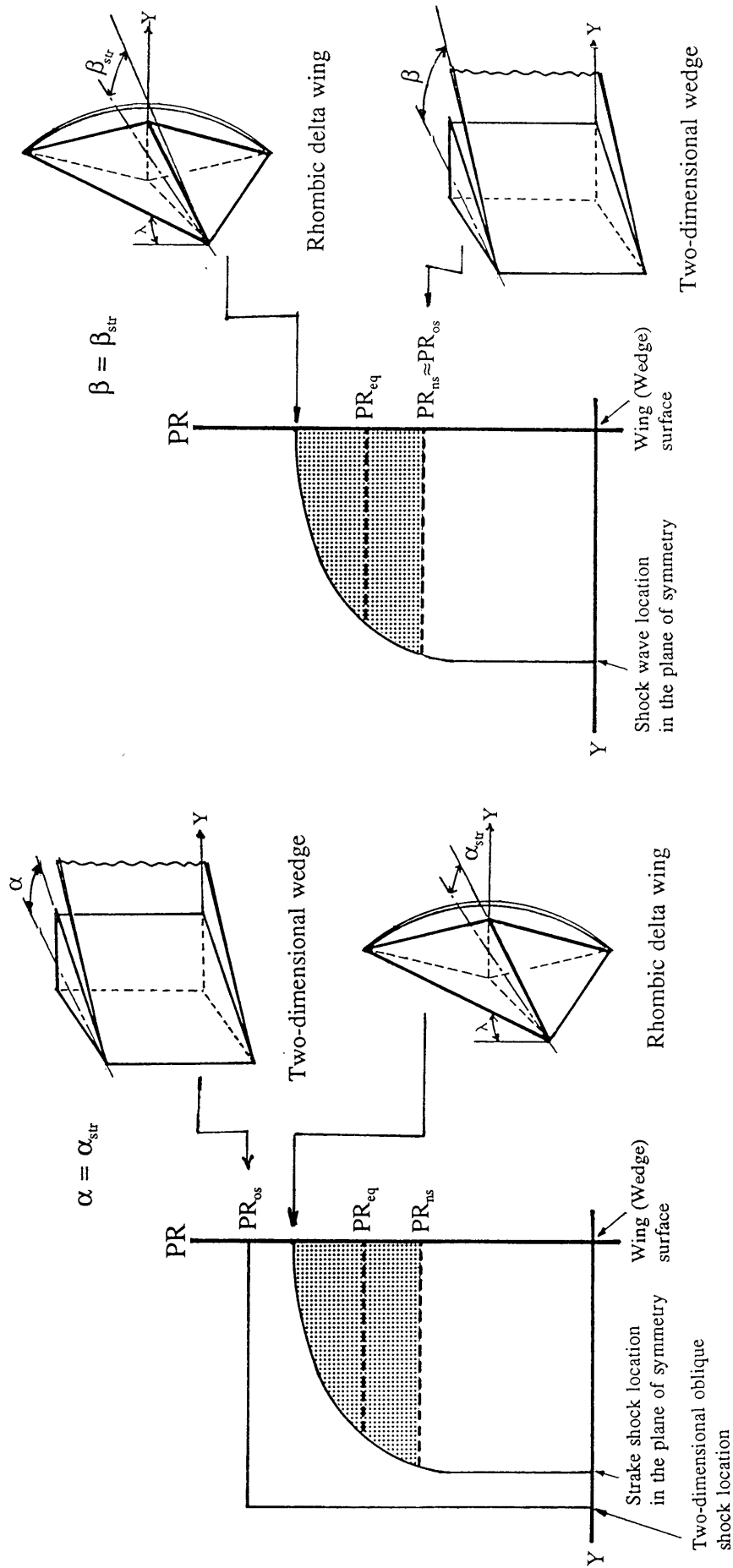


Fig.5-7 Definition of PR_{eq}



(a) With a same half-apex (wedge) angle (b) With a same shock angle

Fig.5-8 Relationship between PR_{eq} , PR_{os} and PR_{ns}

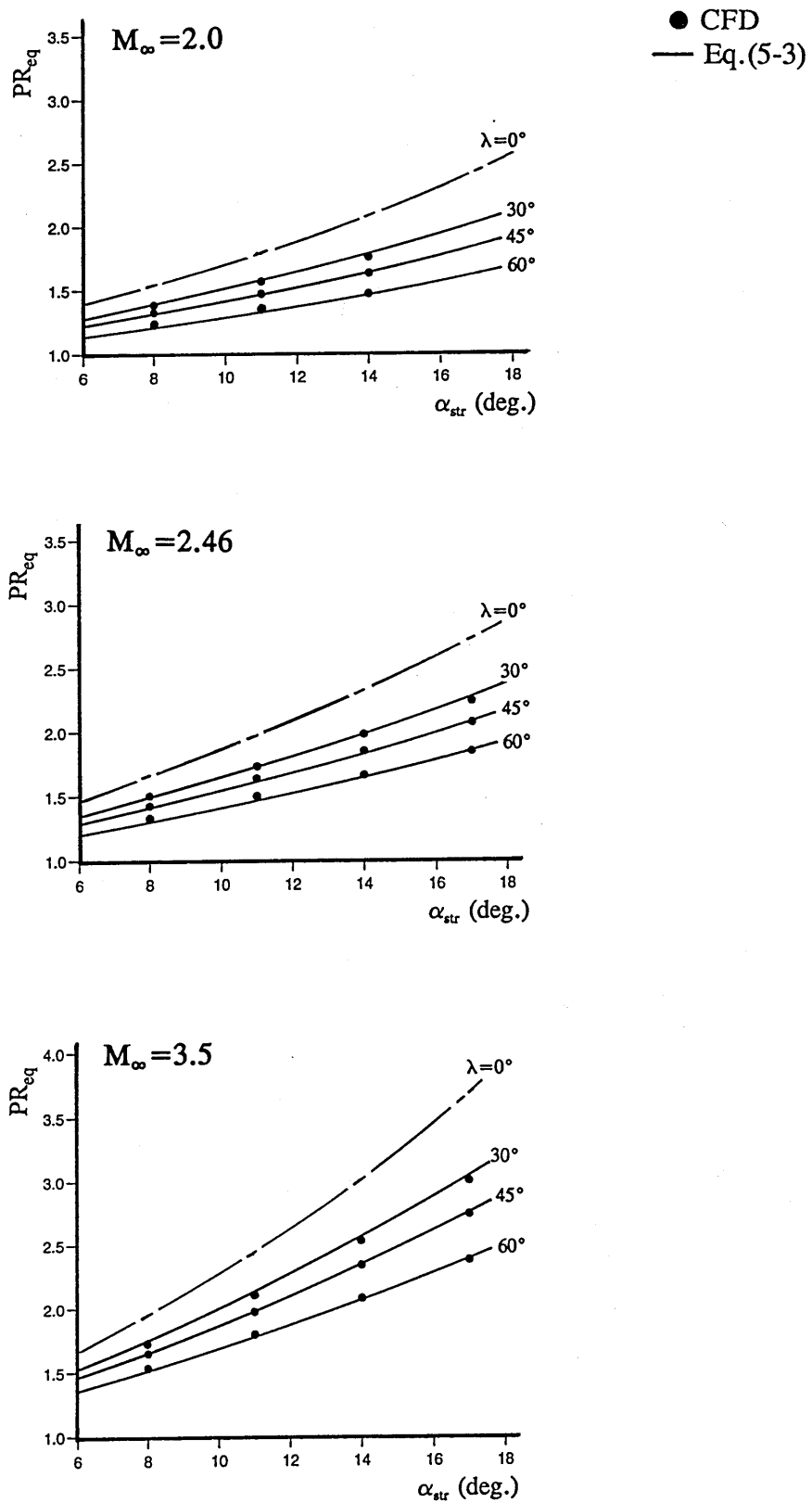
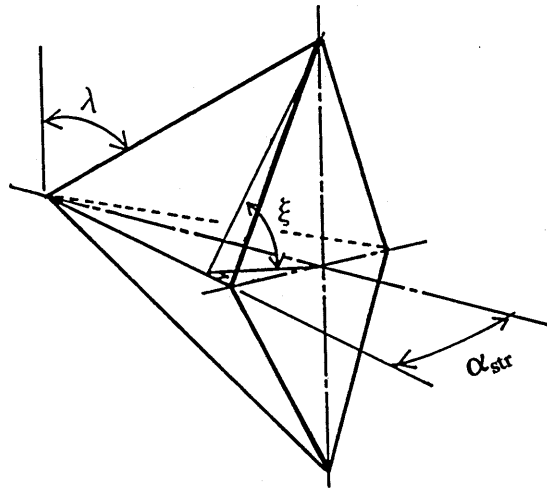
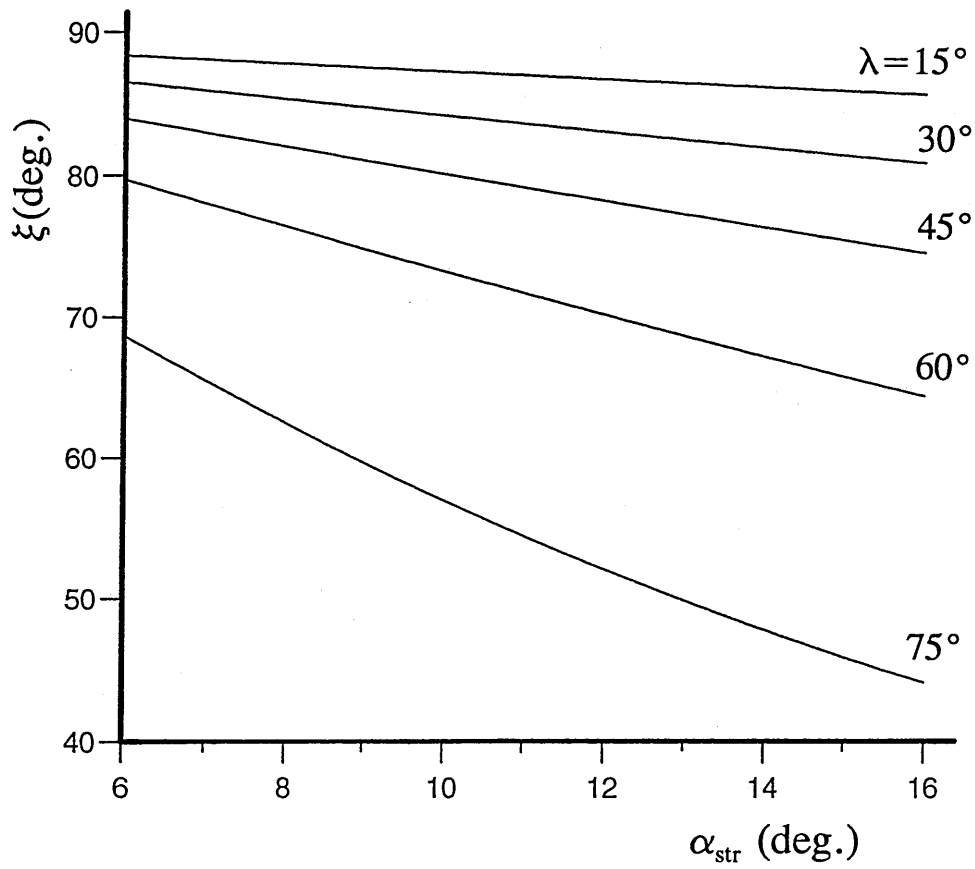


Fig.5-9 Comparison of the equivalent pressure ratio obtained by the CFD procedures and eq.(5-3)

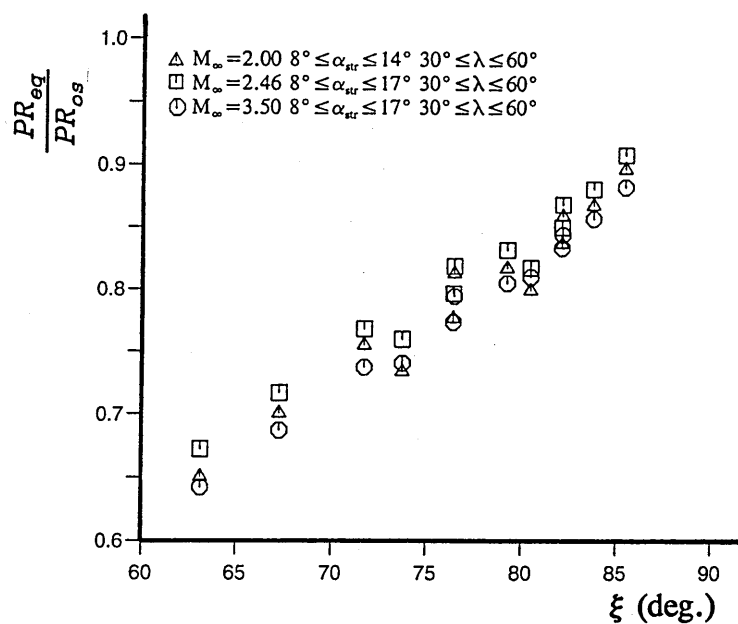


(a) Geometrical parameters of the rhombic delta wing

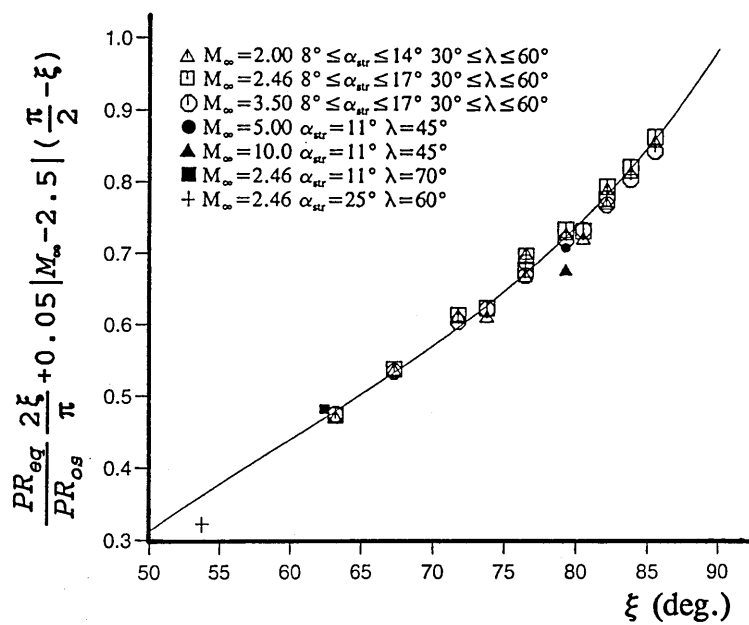


(b) Variation of ξ with λ and α_{str}

Fig.5-10 Geometrical parameters of the rhombic delta wing and their relations

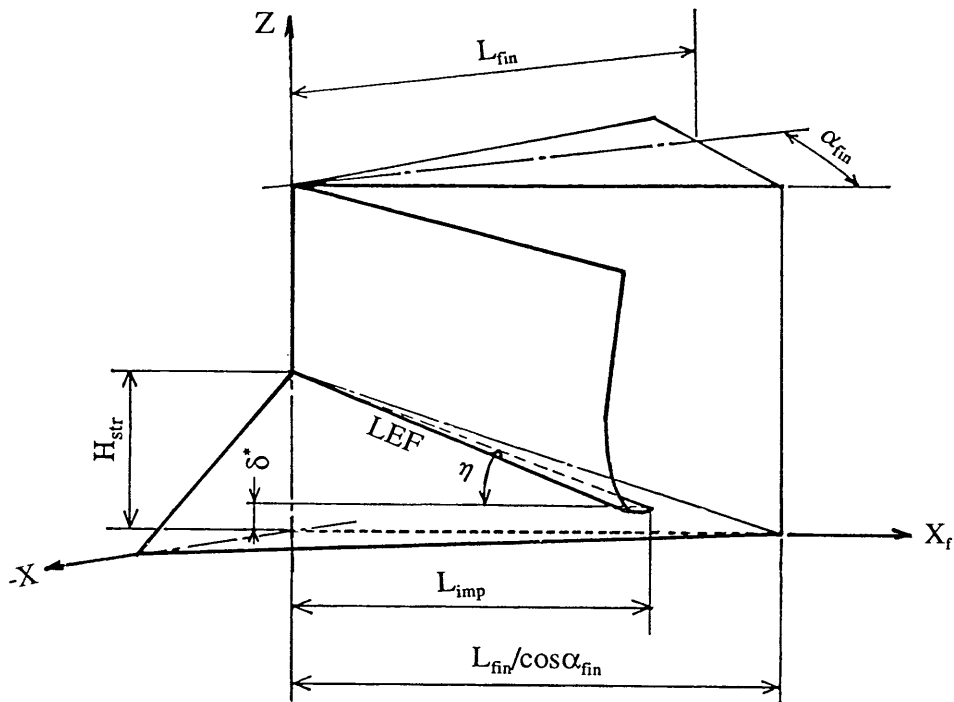


(a) Simple correlation by ξ - PR_{eq}/PR_{os}

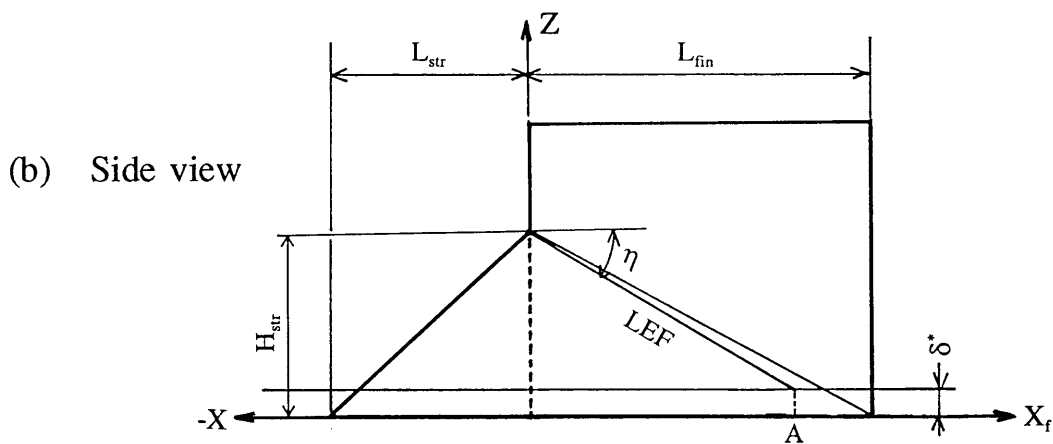


(b) The equivalent pressure ratio correlated by Formula (5-2)

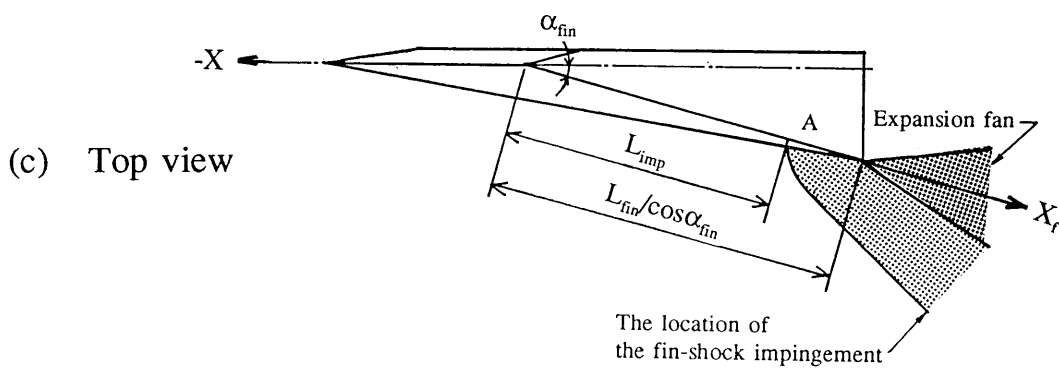
Fig.5-11 Correlation of the equivalent pressure ratio



(a) Three-dimensional view



(b) Side view



(c) Top view

Fig.5-12 Geometrical relation among the model, the fin shock and δ^*

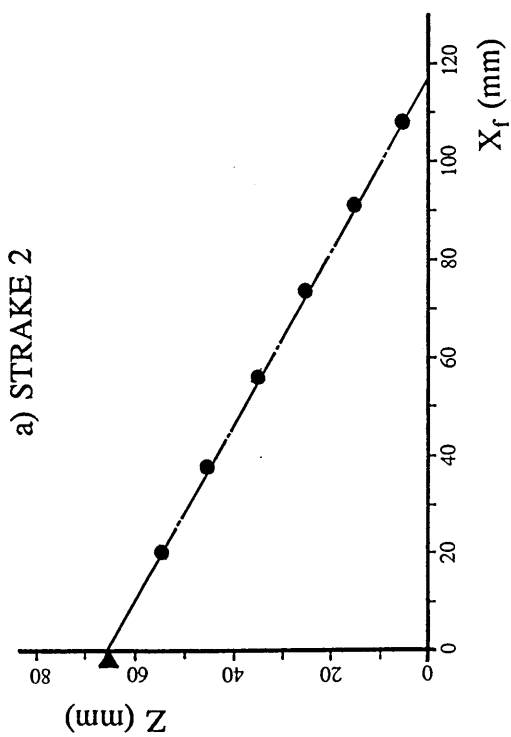
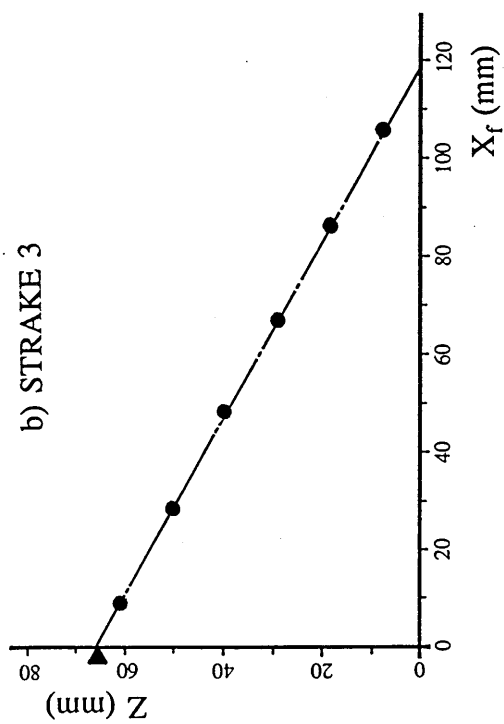
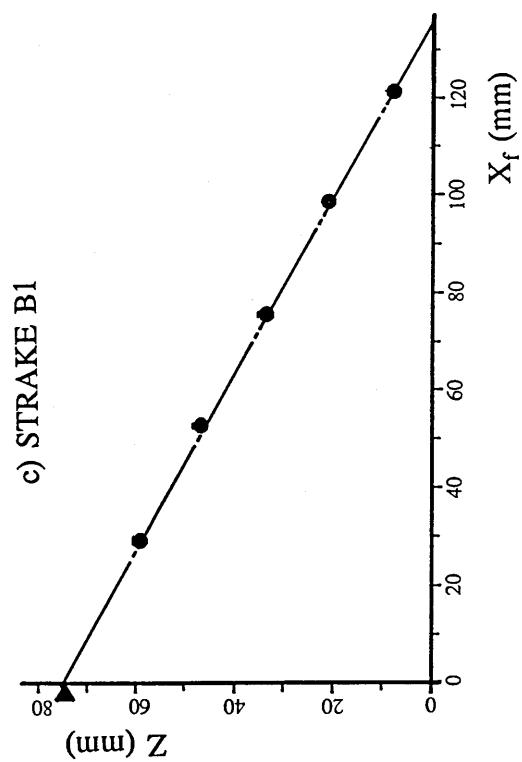
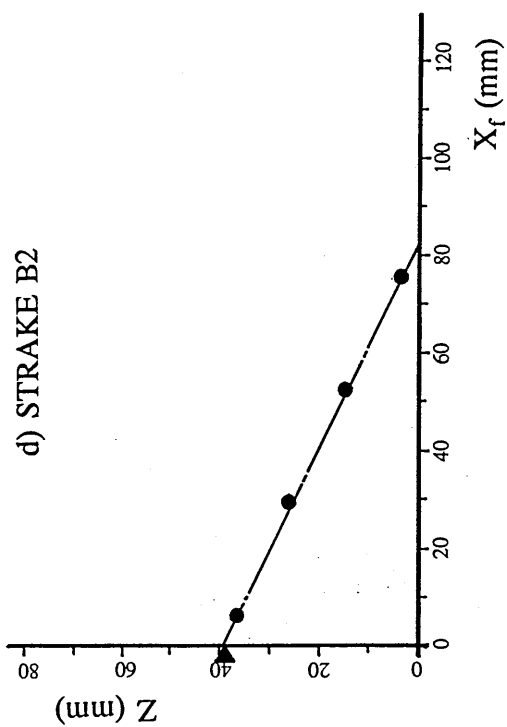


Fig.5-13 The positions of the lower edge of the fin shock numerically obtained

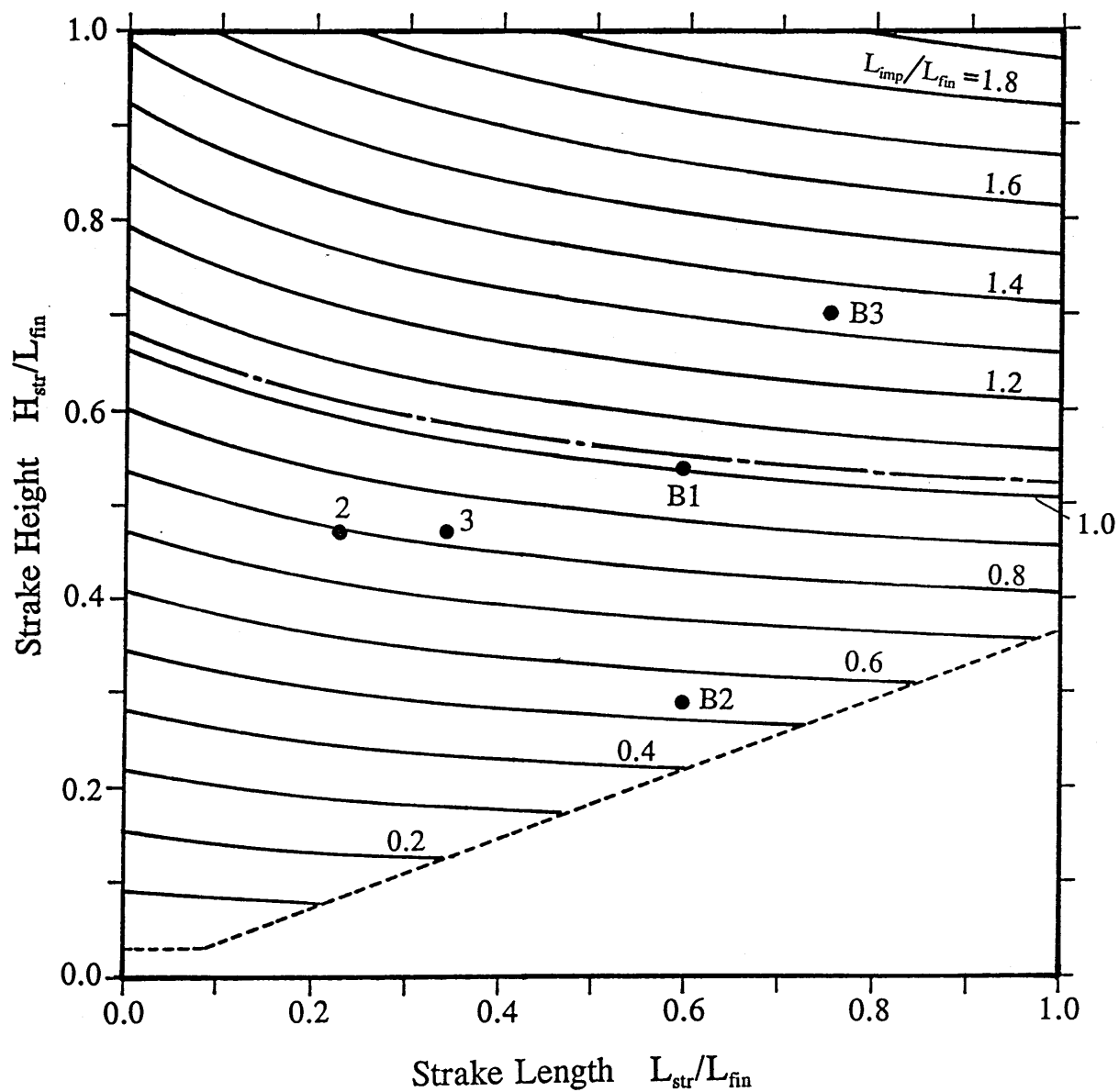


Fig.5-14 Fin-shock impingement location
 $(M_{\infty}=2.46, \alpha_{fin}=15^{\circ}, \delta^*/L_{fin}=0.0273)$

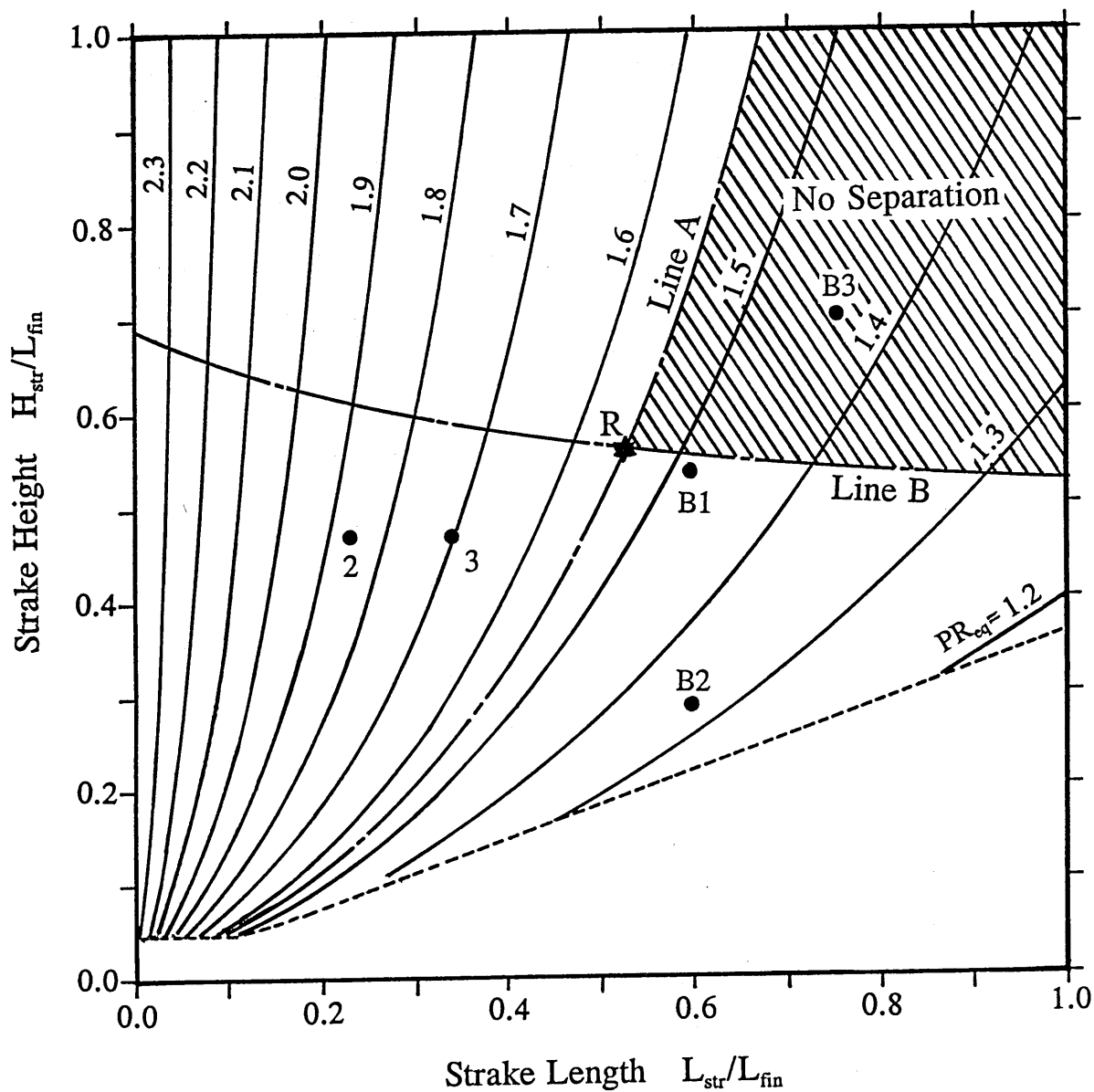
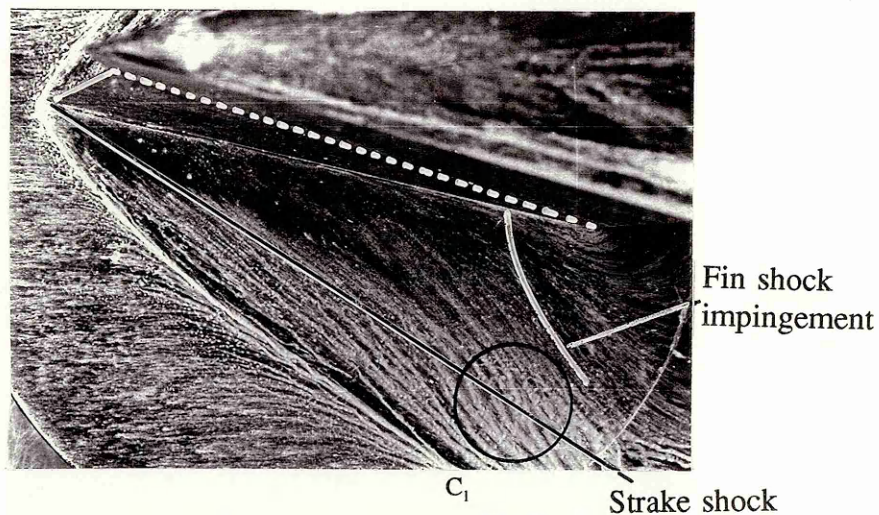
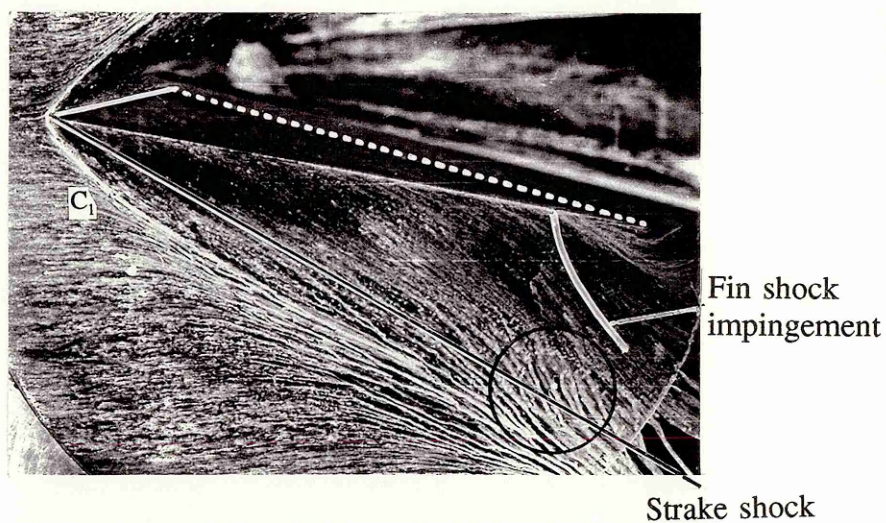


Fig.5-15 Geometrical requirements for the strake with strake-shock pressure levels
 $(M_{\infty}=2.46, \alpha_{fin}=15^{\circ}, \delta^*/L_{fin}=0.0273)$

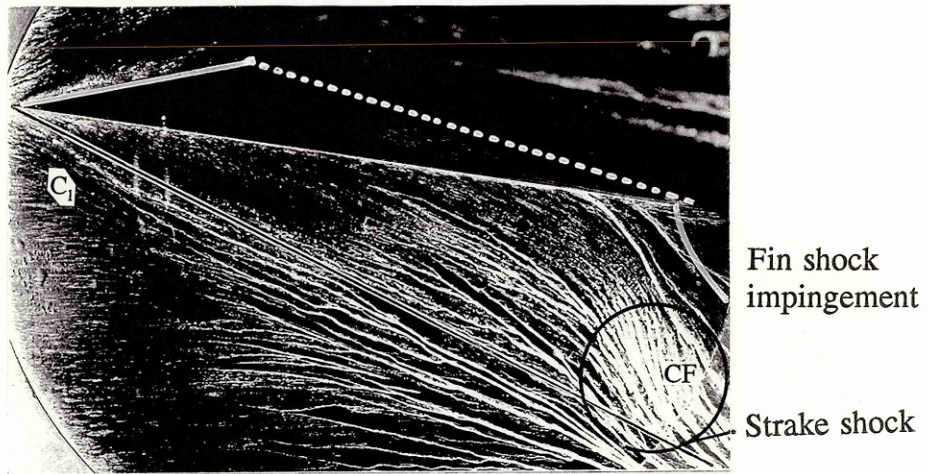


(a) Strake 2 ($PR_{eq}=1.87$, $L_{imp}/L_{fin}=0.79$, $PR_{LEF}=1.13$)

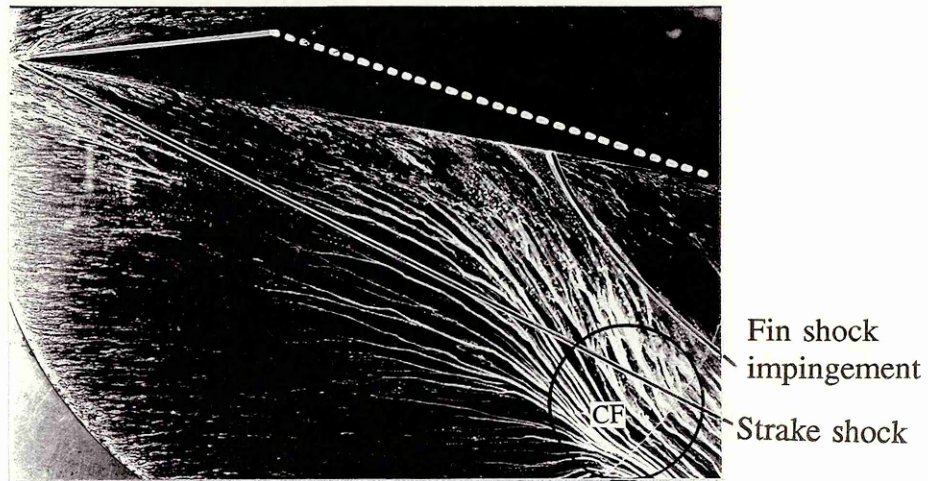


(b) Strake 3 ($PR_{eq}=1.71$, $L_{imp}/L_{fin}=0.83$, $PR_{LEF}=1.20$)

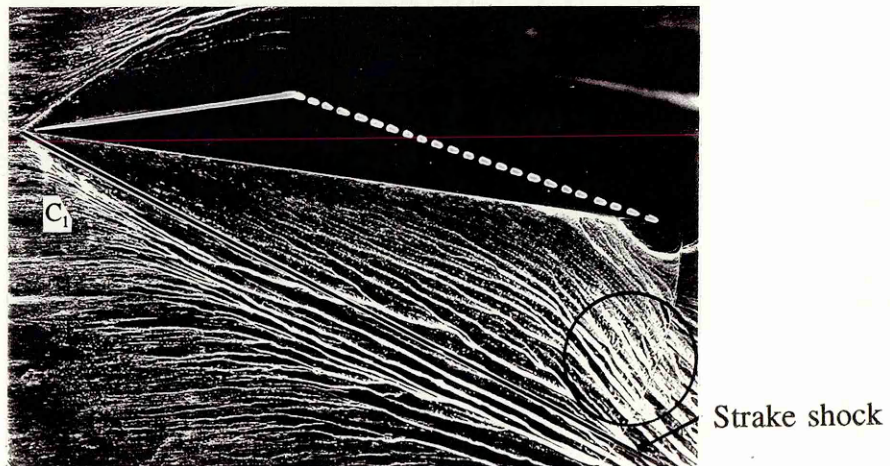
Fig.5-16 Oil flow patterns on the wall with PR_{eq} , L_{imp}/L_{fin} and PR_{LEF}



(c) Strake B1 ($PR_{eq}=1.49$, $L_{imp}/L_{fin}=1.00$, $PR_{LEF}=1.35$)



(d) Strake B2 ($PR_{eq}=1.33$, $L_{imp}/L_{fin}=0.52$, $PR_{LEF}=1.40$)



(e) Strake B3 ($PR_{eq}=1.44$, $L_{imp}/L_{fin}=1.34$, $PR_{LEF} = -$)

Fig.5-16 Continued

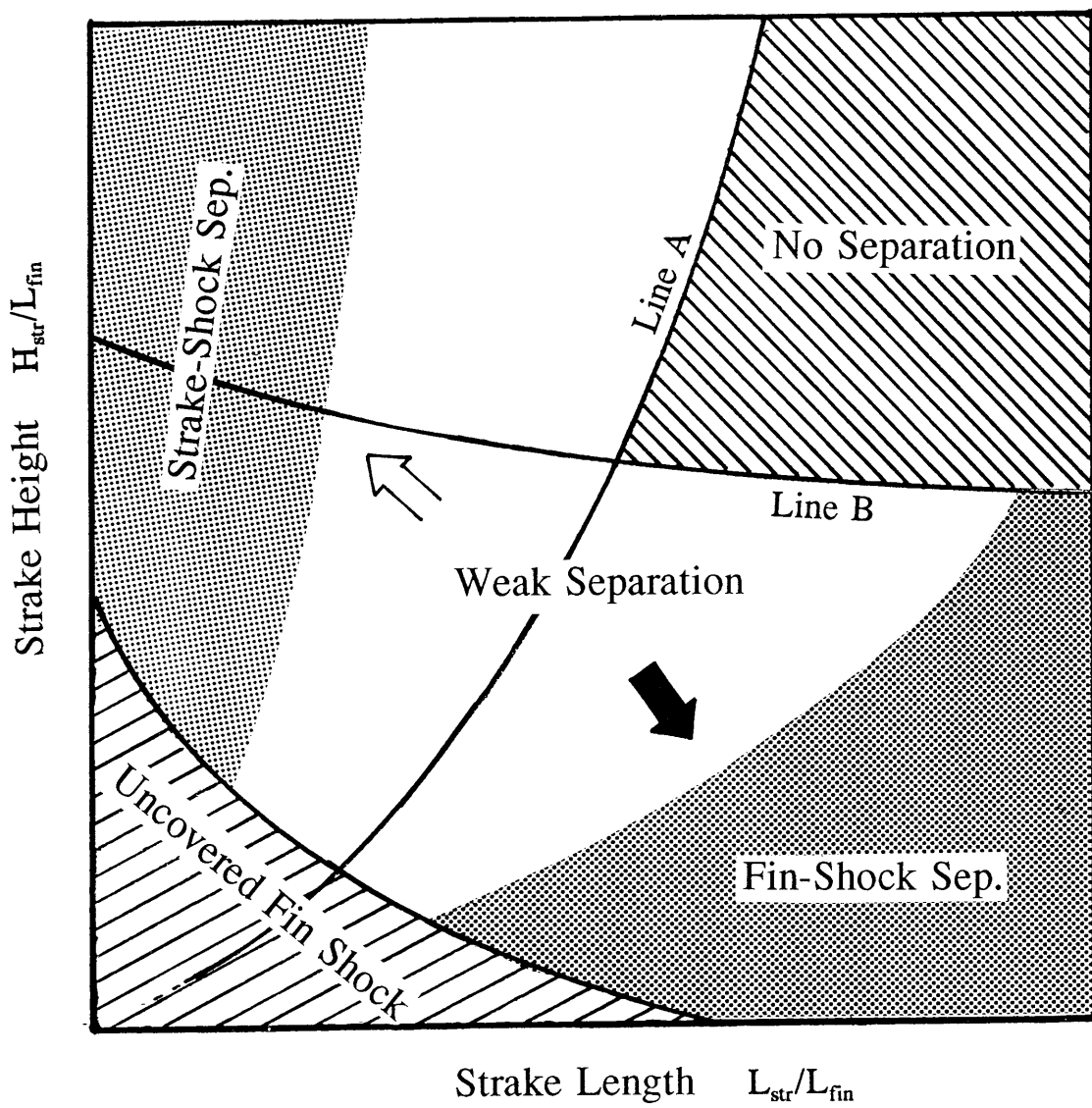


Fig.5-17 Regimes of strake-geometry effects on separation

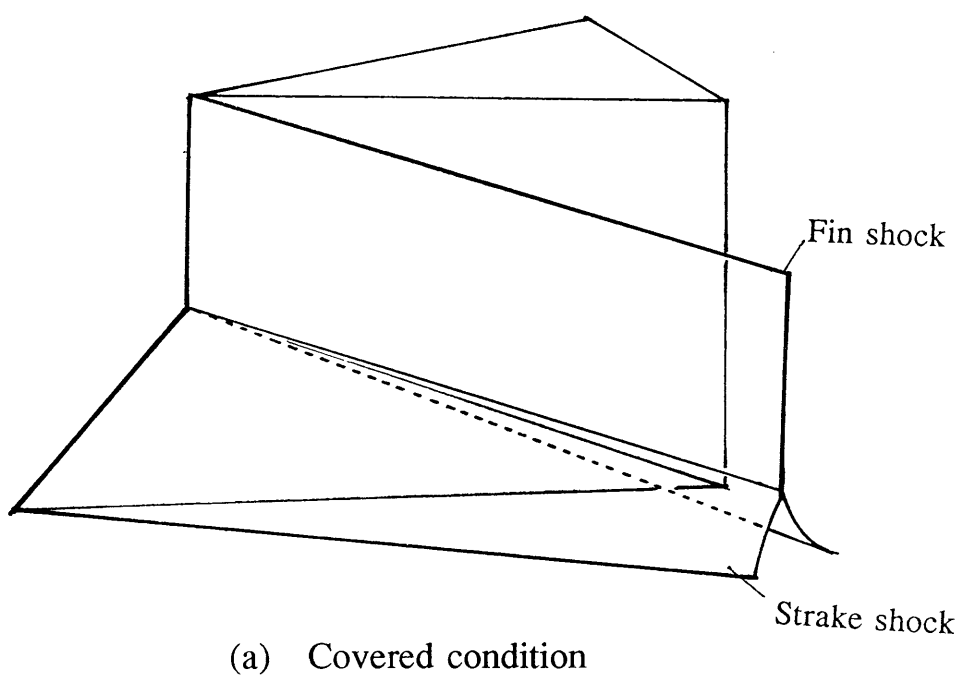
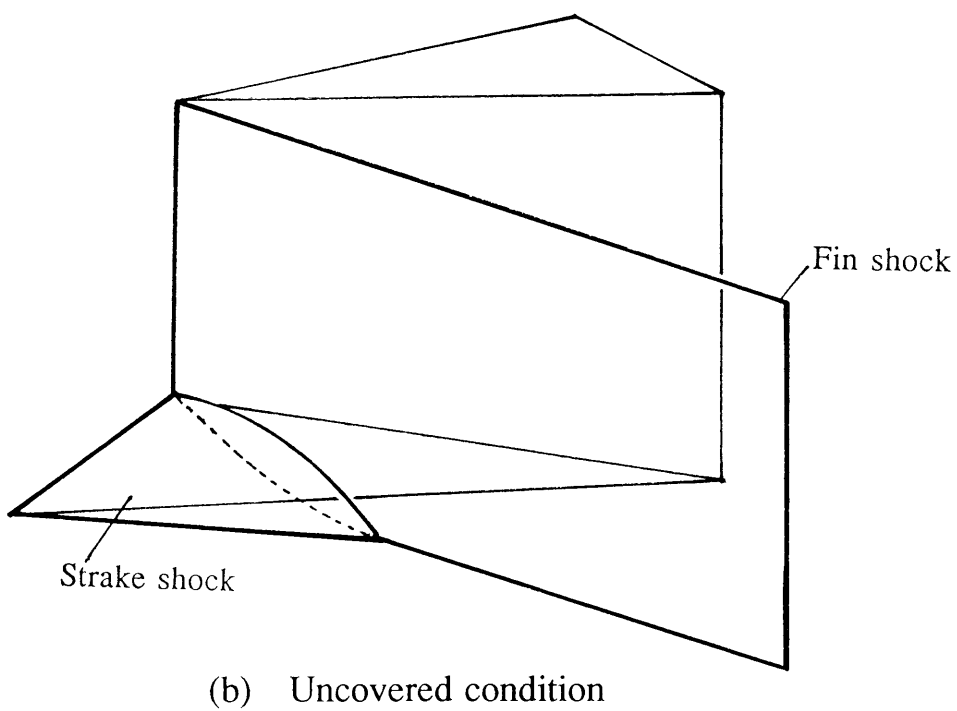
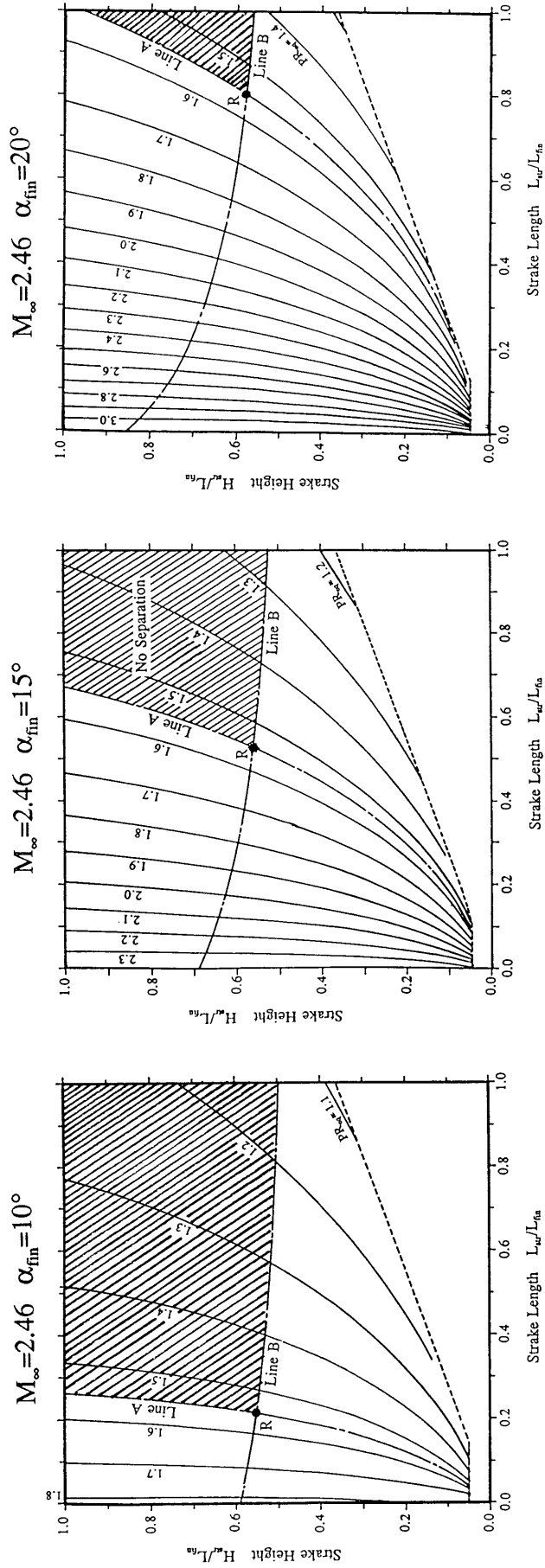
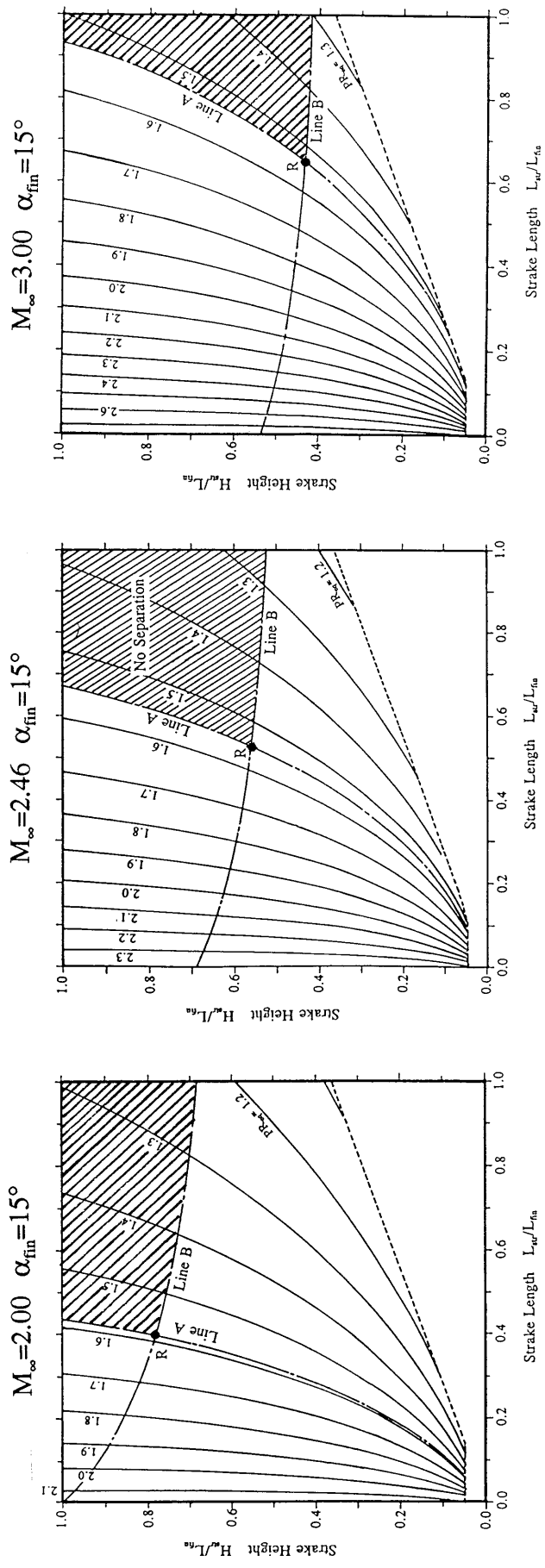


Fig.5-18 "Covered" and "Uncovered" fin shocks



(a) Effect of α_{fin}

Fig.5-19 Effects of α_{fin} and M_∞ on the strike-shock pressure levels and geometrical requirements



(b) Effect of M_∞

Fig.5-19 Continued

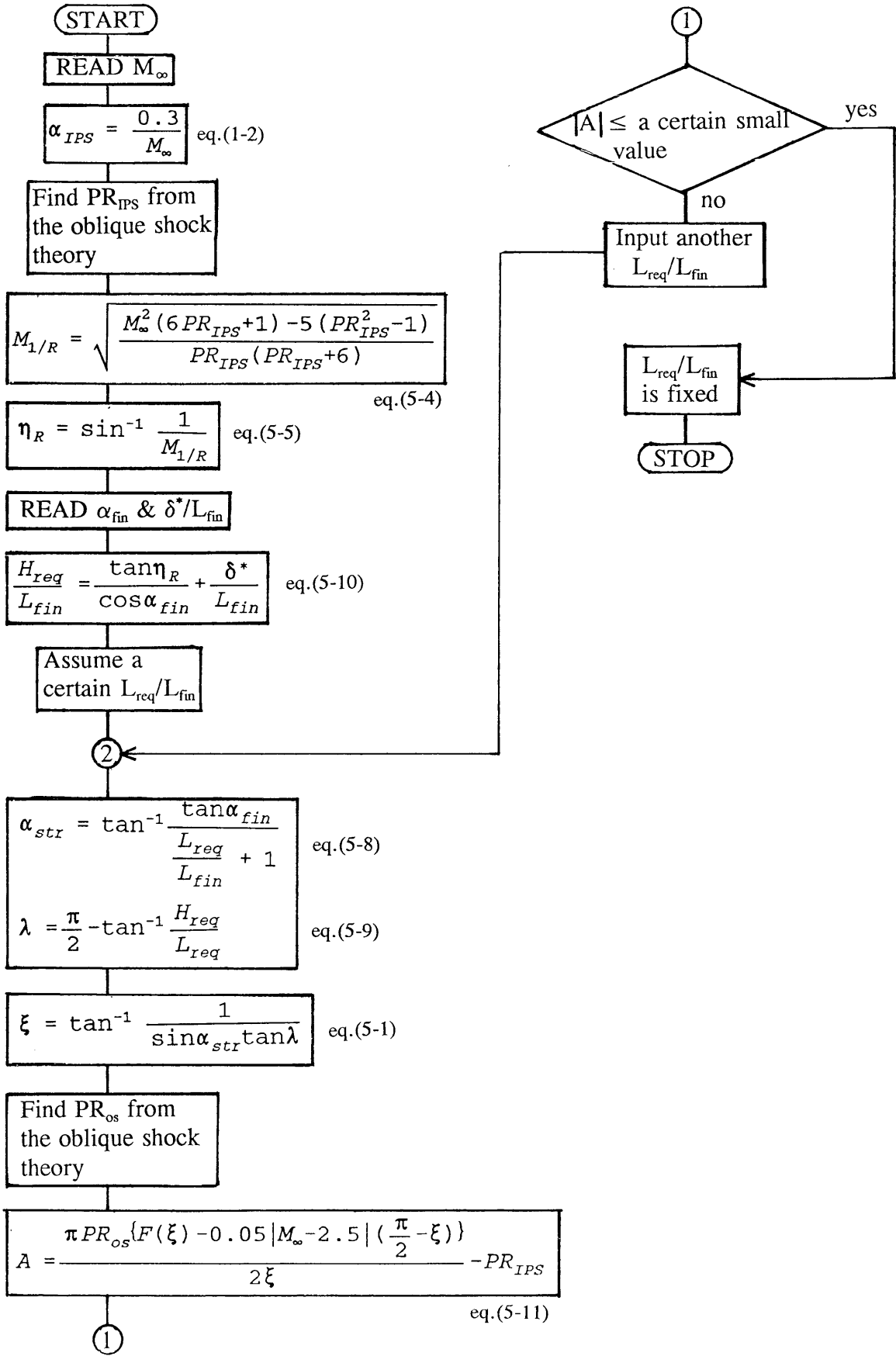
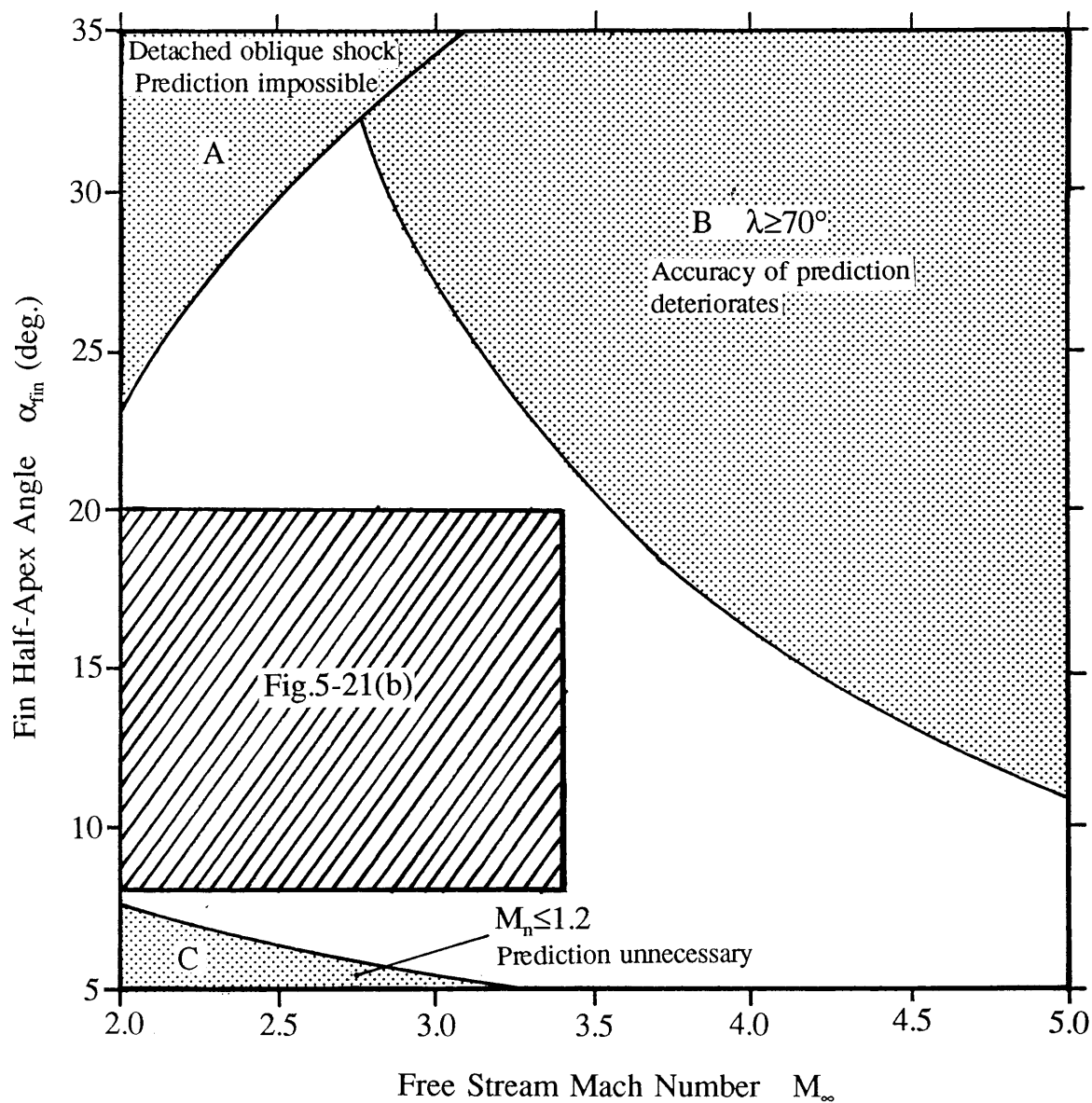
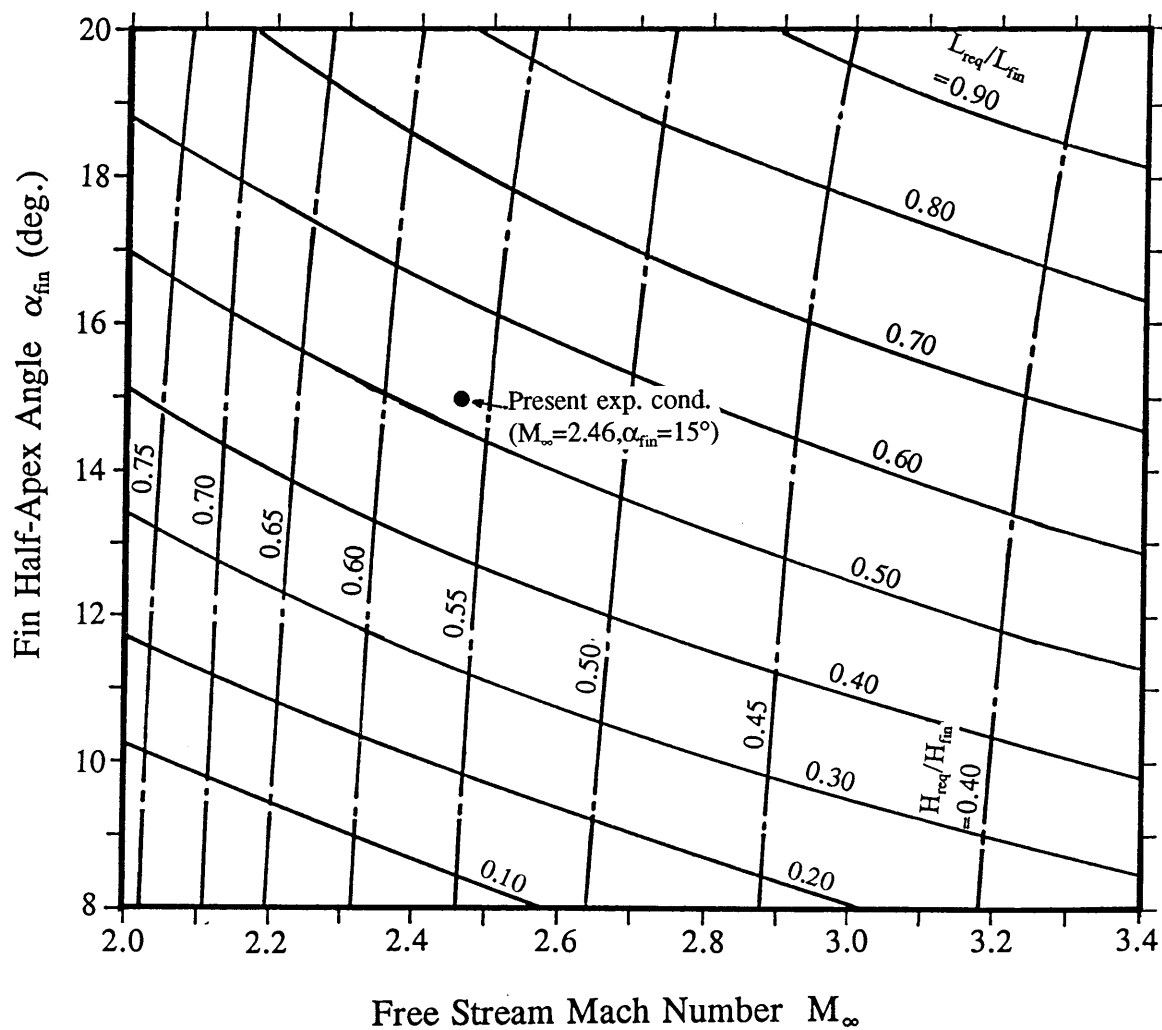


Fig.5-20 Calculation flow chart for L_{req} and H_{req}



(a) The applicable range of the prediction

Fig.5-21 Effects of M_∞ and α_{fin} on the required strake geometries, L_{req} and H_{req}



(b) The values of L_{req}/L_{fin} and H_{req}/L_{fin} predicted

Fig.5-21 Continued

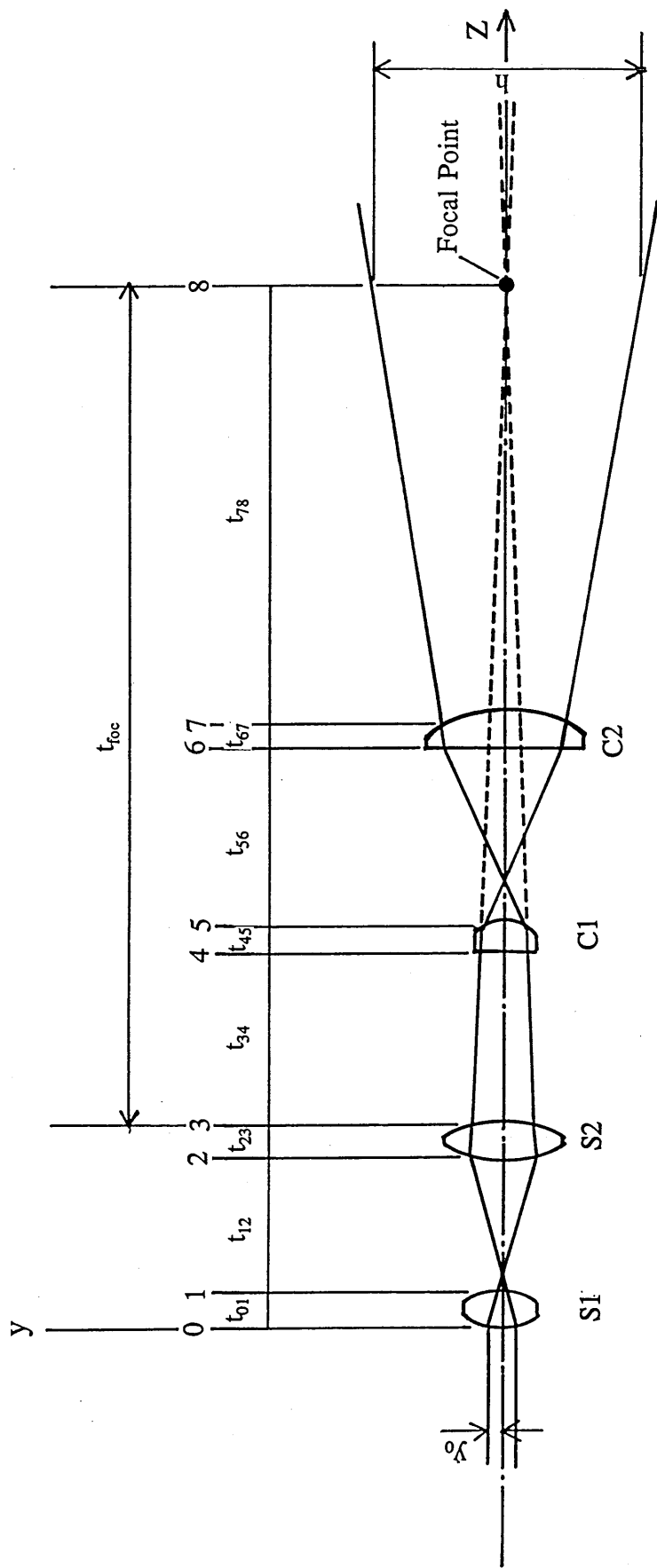


Fig.A-1 Combination of the optical components and the coordinate system

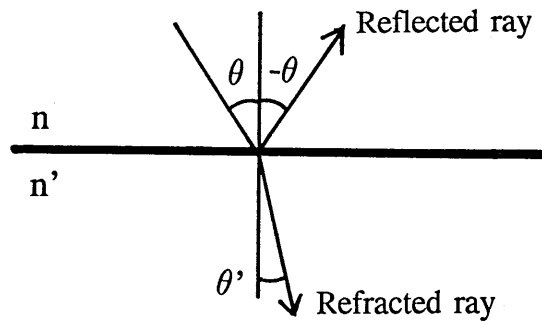


Fig.A-2 Reflection and refraction at an interface (Ref.A1)

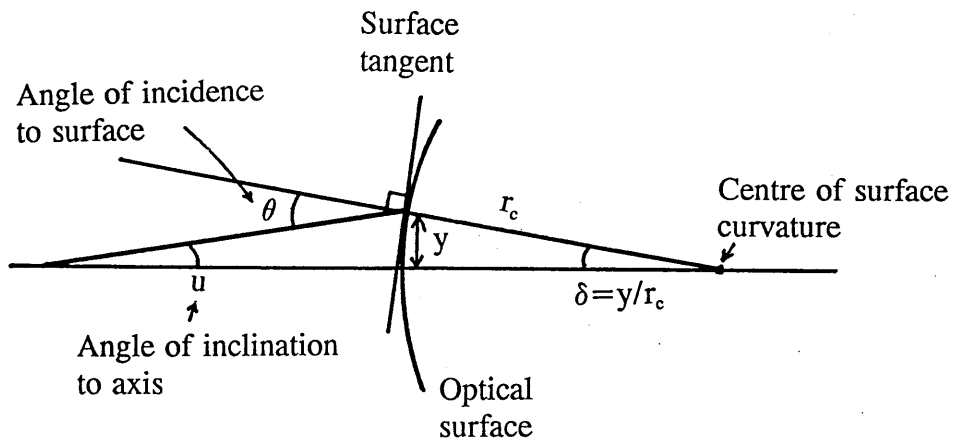
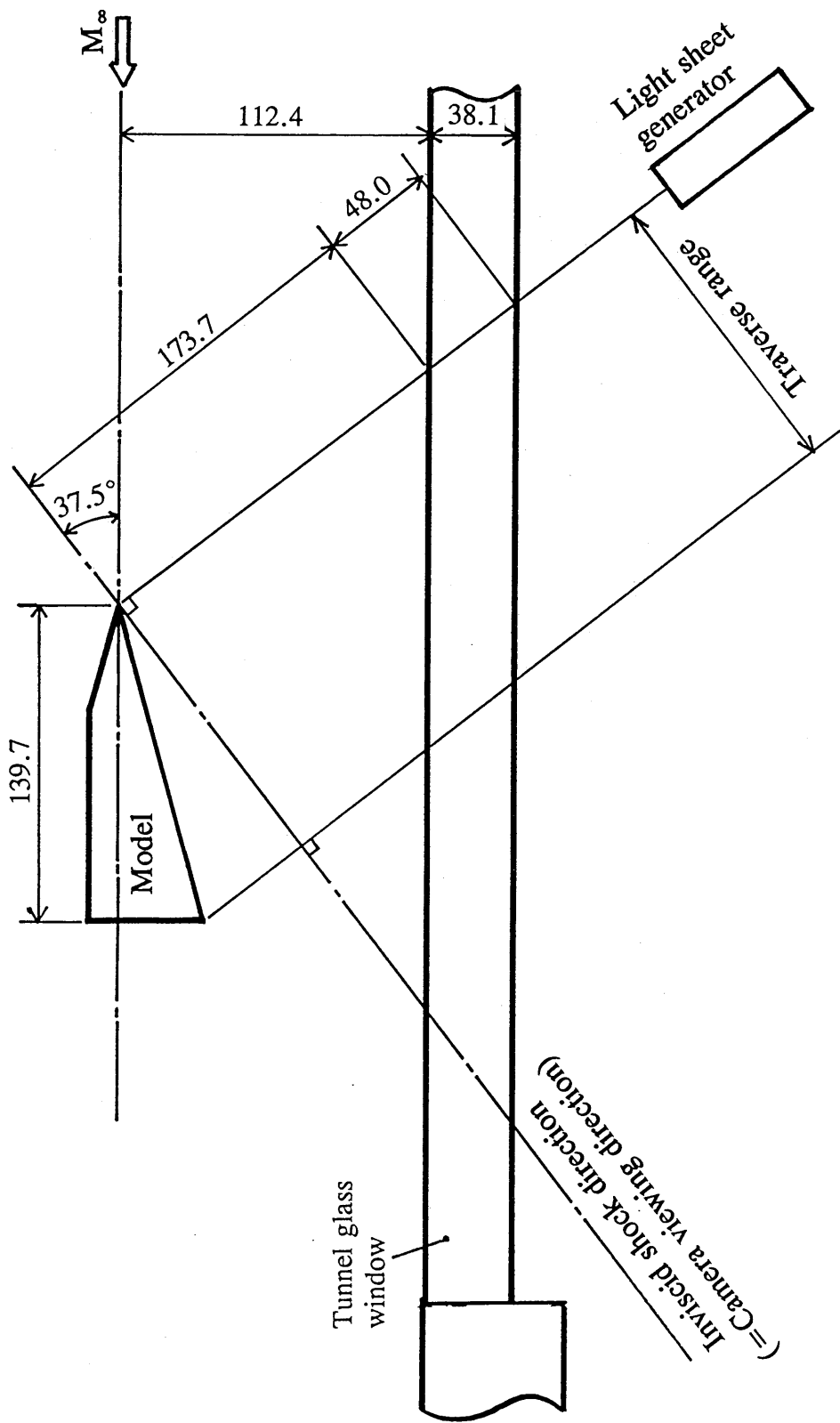
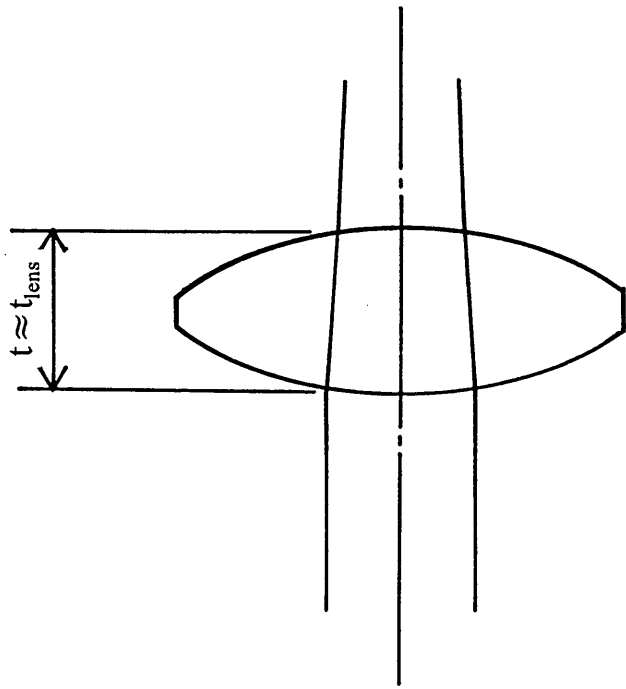


Fig.A-3 Illustration of the angle of an optical surface made by a paraxial ray (Ref.A1)

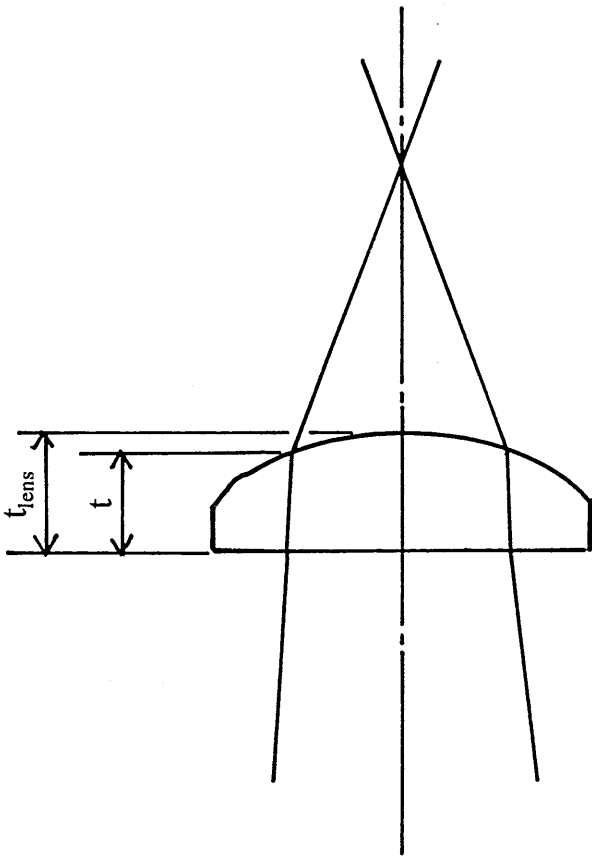


UNIT : mm

Fig.A-4 Geometrical relations for the model, the tunnel glass window and the light-sheet generator



(a) Spherical lens



(b) Cylindrical lens

Fig.A-5 The distance between the points of entry and departure of a light beam passing through a lens

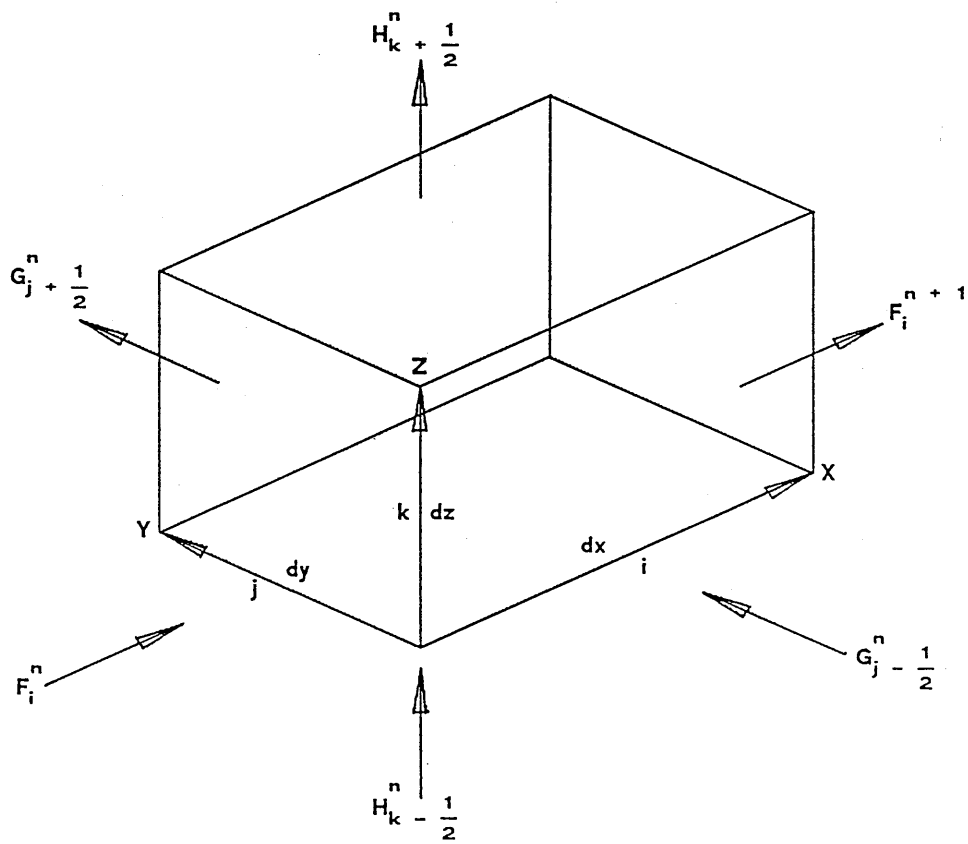
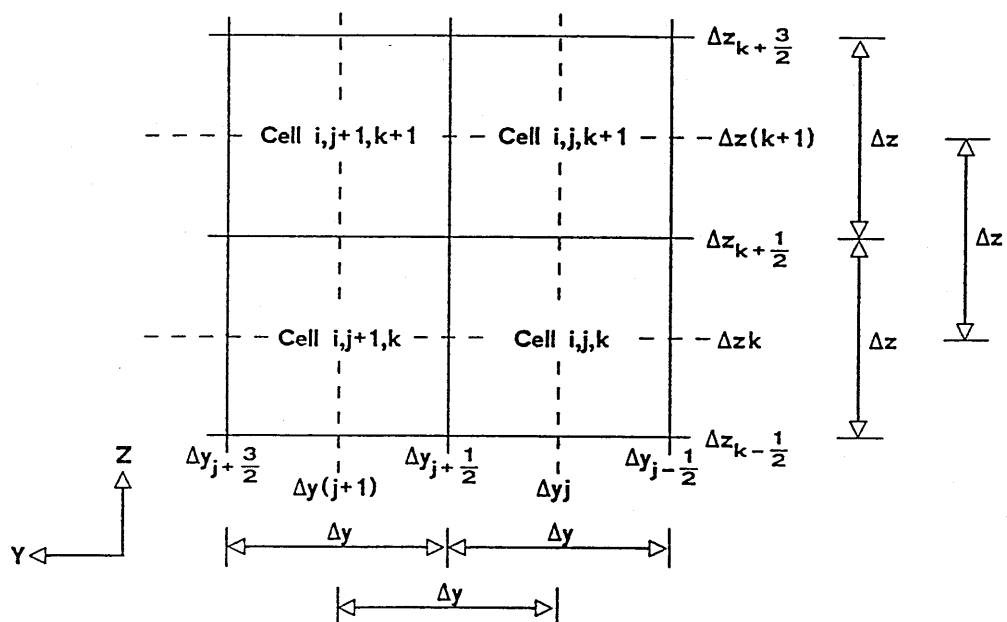
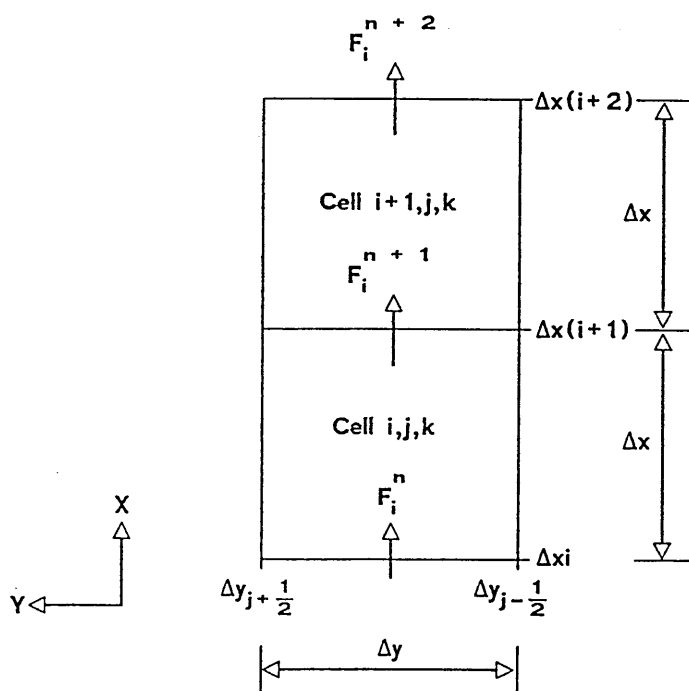


Fig.B-1 Control volume for computational cell i,j,k (Ref.79)

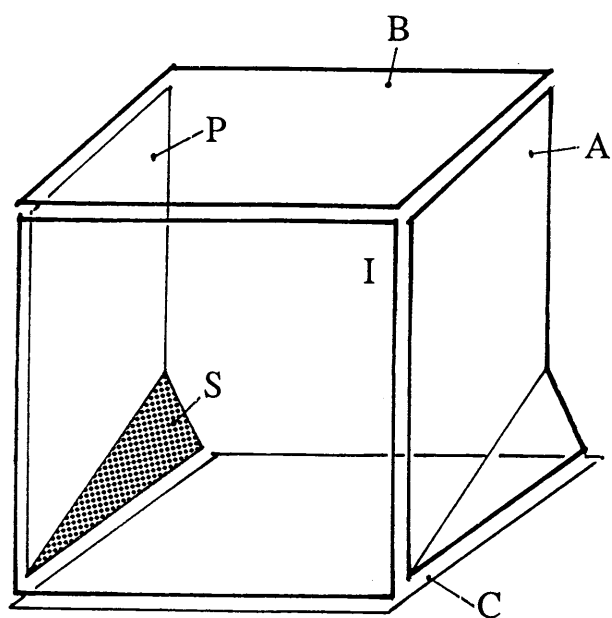


(a) 2D slice of four cells in the YZ plane

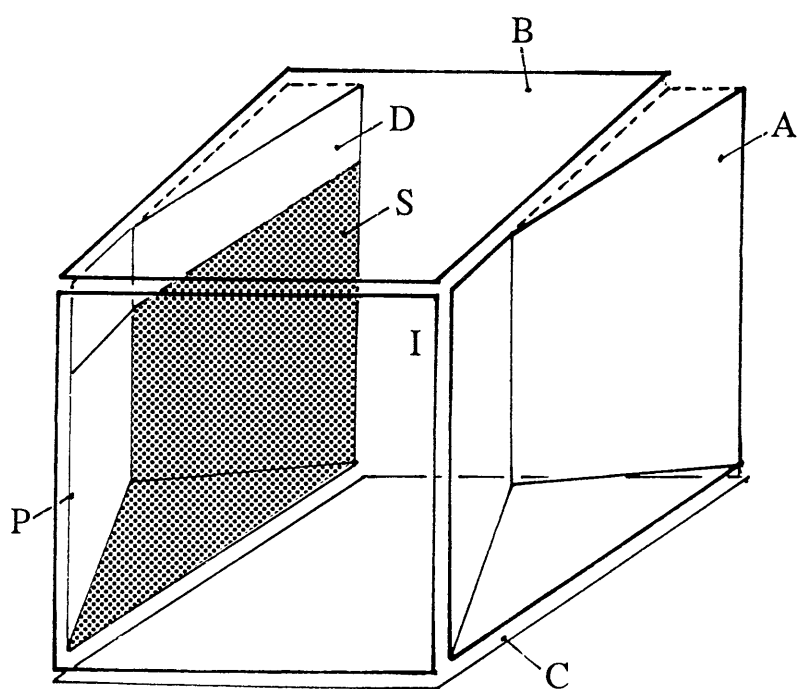


(b) Cell marching direction

Fig.B-2 Relation with neighbouring cells (Ref.79)



(a) Rhombic delta wing



(b) Strake + Fin

Fig.B-3 Computational field and its initial & boundary conditions

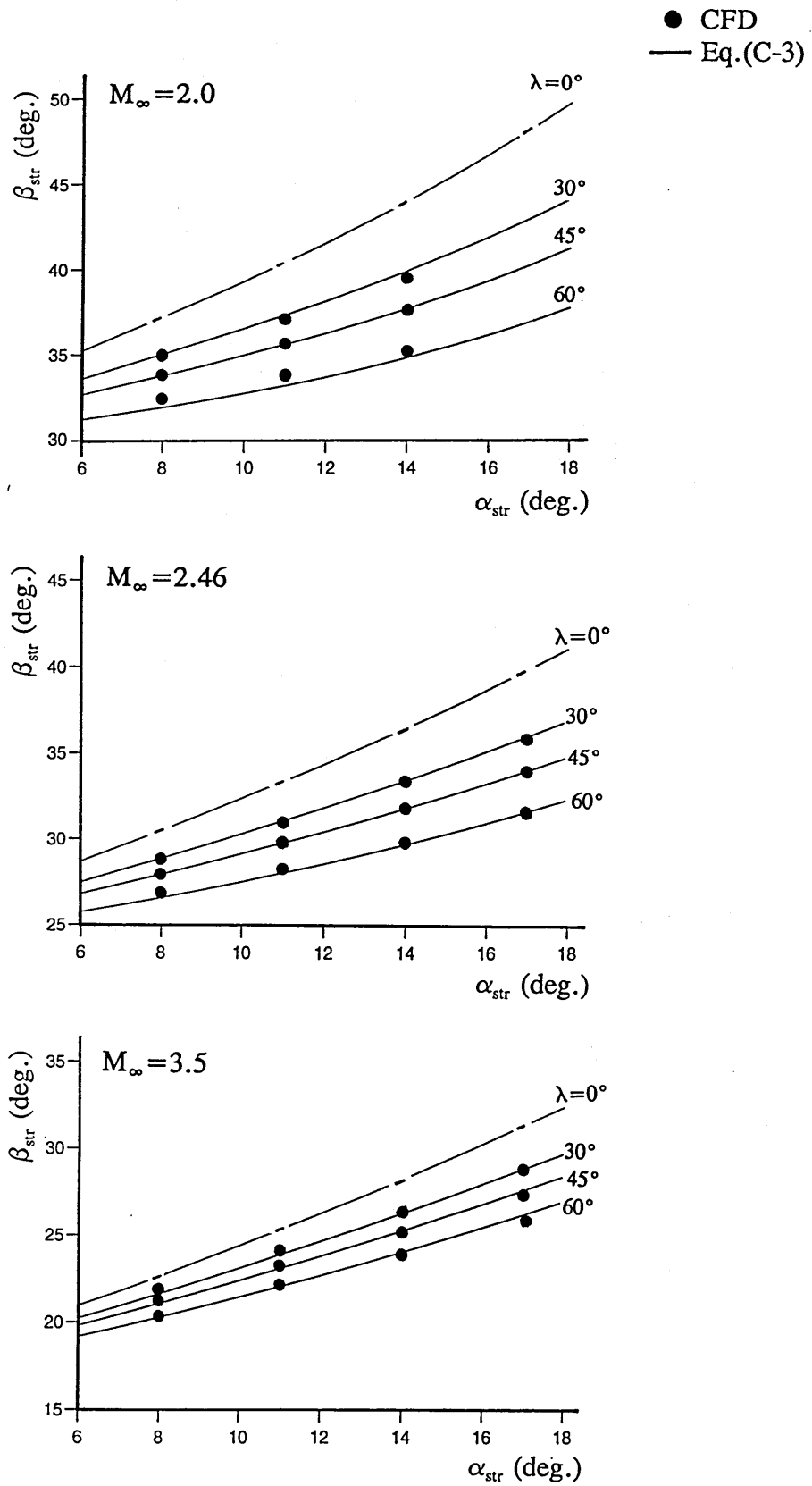
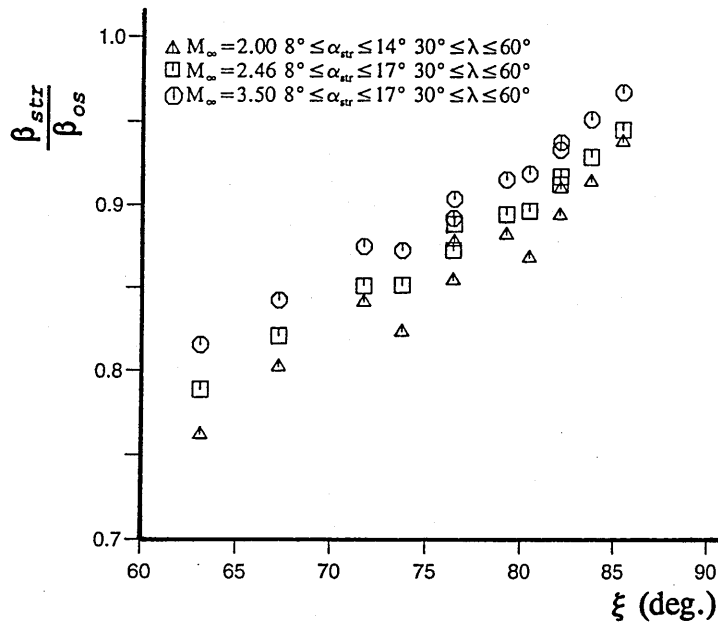
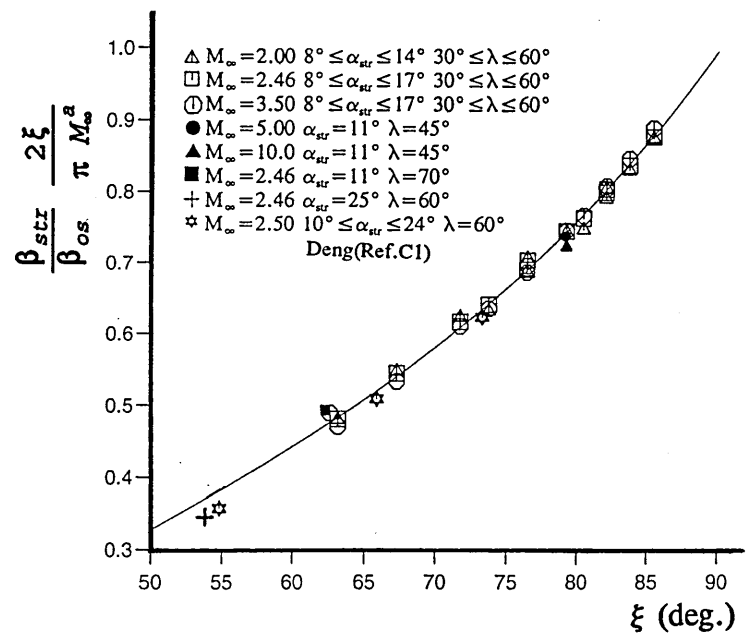


Fig.C-1 Comparison of the strake-shock wave angles obtained by the CFD procedures and eq.(C-3)



(a) Simple correlation by $\xi - \beta_{str}/\beta_{os}$



(b) The stroke-shock angles correlated by formula (C-1)

Fig.C-2 Correlation of the stroke-shock angle

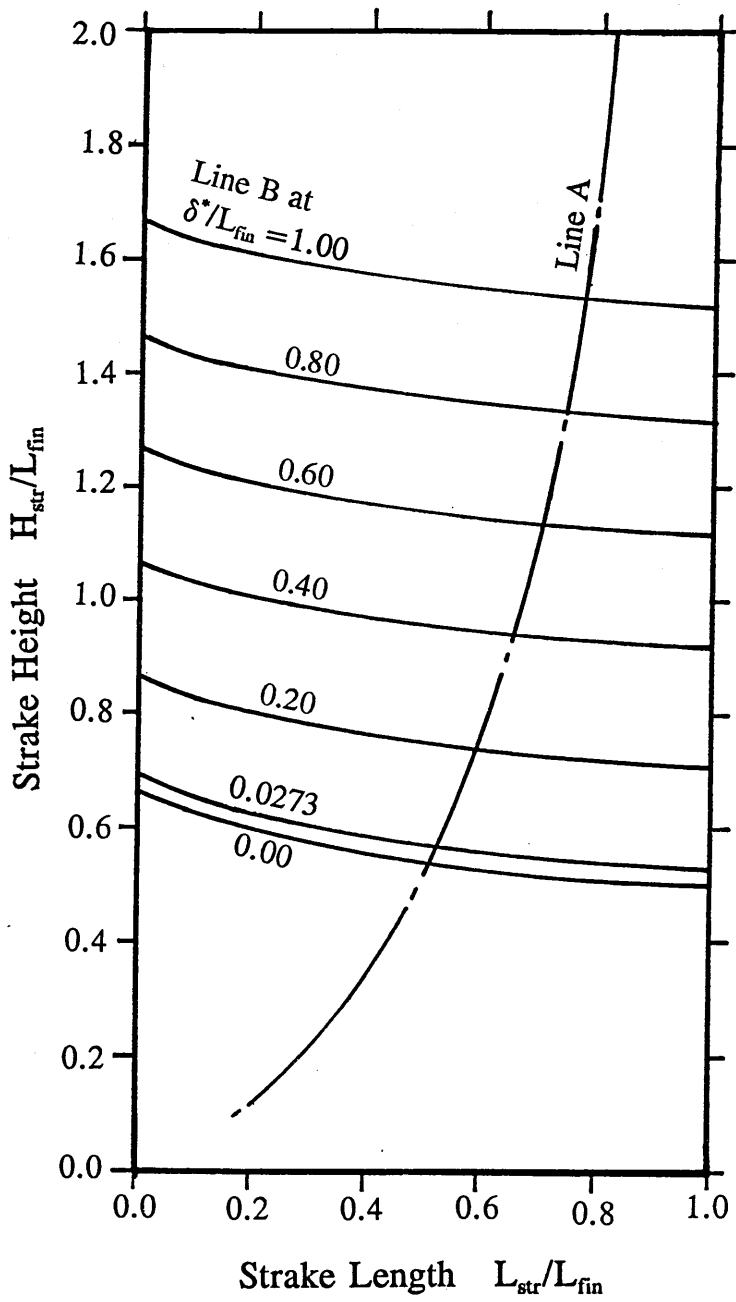


Fig.D-1 Effect of δ^*/L_{fin} on the geometrical requirements

($M_\infty = 2.46$, $\alpha_{fin} = 15^\circ$)

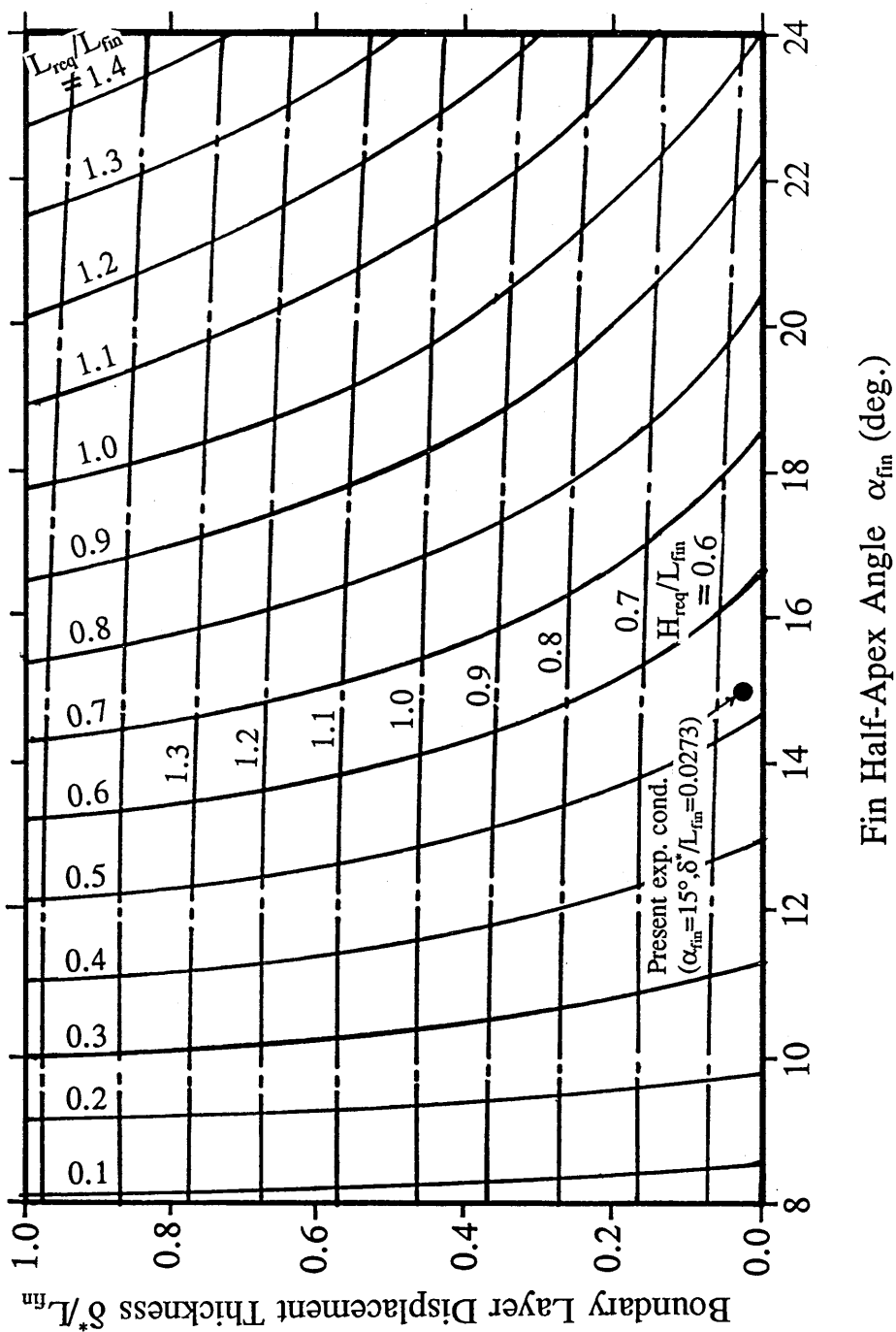


Fig.D-2 Effects of α_{fin} and δ^*/L_{fin} on the required stake geometries

($M_\infty = 2.46$)

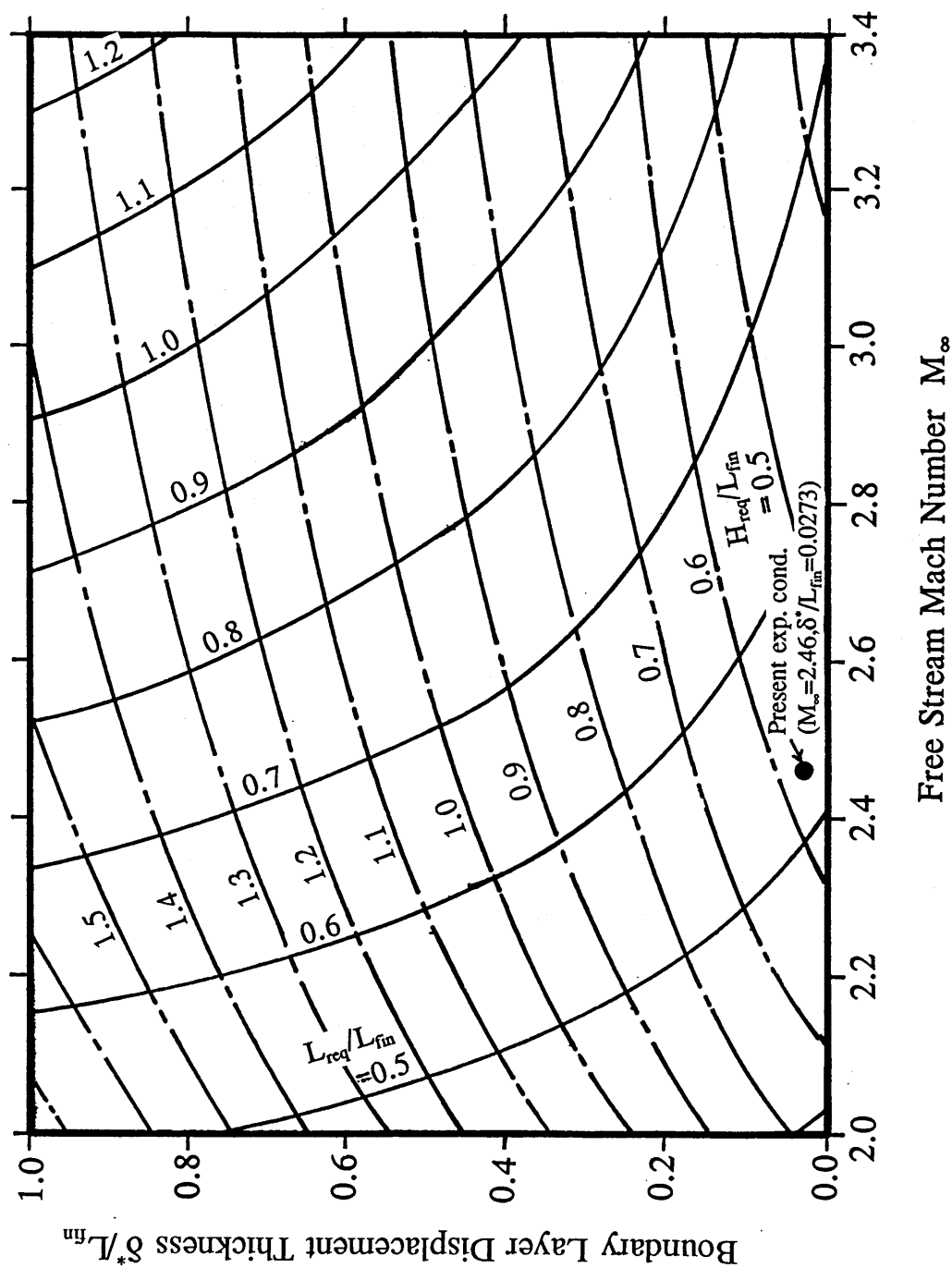


Fig.D-3 Effects of M_∞ and δ^*/L_{fin} on the required strake geometries
 $(\alpha_{fin} = 15^\circ)$