The Possibilities Of Sailplanes Designed To Use Laminar Flow Control

By A.M.O. SMITH

Huge Increases in Sailplane Performance (L/D Ratios as High as 106:1) Are Theoretically Achievable Now

About the author. . .

A. M. O. Smith, longtime Chief Aerodynamics Engineer—Research for Douglas Aircraft Co. at Long Beach, California, took three degrees from California Institute of Technology: a B.S. in mechanical engineering and Masters degrees in mechanical and aeronautical engineering. In 1938 he joined Douglas to do aerodynamics work on the A-20 Havoc attack bomber; he later did the aerodynamics on the Douglas Skystreak which captured the world speed record. After working on the high-performance Skyray he shifted emphasis to research and spent the balance of his career in that discipline. He still consults for Douglas. This article grew out of a talk given before the Southern California Soaring Association.

ABSTRACT

This paper is basically an aerodynamic study of the possible gains in performance that could be made by applying laminar flow control to obtain more extensive laminar layers than those existing naturally. Several of the more practical problems such as diameter of a windmill to run the pump are also discussed. While the paper looks well ahead to assess the prospects, it means to be realistic and not to bypass many problems. As an example of the realism no laminar flow control is applied to the tail surfaces. Yet if someday successful application is made to the wing, undoubtedly there would be serious thoughts about applying LFC to tail surfaces. It is found that for a sailplane of the general quality and characteristics of the *Nimbus* 3 or AS-W 22 the L/D_{MAX} could be increased to about 76. The Griffith airfoil was investigated as an alternate type to help solve the problems of fabrication. Although the Griffith looked very good when flow losses were not considered, when they were indeed considered it was definitely inferior to the conventional laminar flow control system. However the system is so little explored that considerable study is recommended to see if it cannot be substantially improved.

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PRINCIPAL NOMENCLATURE

a = a measure of disk loading for the windmill, eq (a1)
AR = aspect ratio, b^2/s
b = wing span
c = wing chord
C_D = drag coefficient, C_{D_p} = parasite drag coefficient
C_f = skin friction coefficient
C_L = lift coefficient
C_p = pressure coefficient, $(p-p_\infty)/\frac{1}{2}\rho U_\infty^2$
C_Q = suction coefficient, $Q/S\,U_\infty$ or Q/cU_∞
D = drag, D_t total drag of pumping system
D_{WM} = drag of windmill
e = span efficiency factor
f = parasite drag area, $f = C_{D_p} S$
H = total head, H_2 = total head at Sta. 2, H_3 = total head at Sta. 3
K = a factor involved in the pumping power analysis eq. (a12)
p = pressure
Q = suction flow rate, cu.ft./sec. in English units
R = windmill radius
R_c = chord Reynolds number $U_\infty c/\nu$
R_x = x Reynolds number Ux/ν
R_{δ^*} = boundary-layer displacement thickness Reynolds number $U\delta^*/\nu$
S = wing area
U = velocity outside the boundary layer, U_∞ = flight velocity
u = velocity in a boundary layer
V = velocity of discharge for the air sucked from the boundary layer
v = velocity normal to wing surface, v_o = velocity though the surface.
w_s = sinking speed of the glider
x = distance in the flow direction
y = distance normal to a surface

GREEK

δ^* = displacement thickness of the boundary layer
η = product of pump and windmill efficiencies
η_p = pump efficiency
η_{WM} = windmill efficiency

GAINS FROM LAMINARIZATION

At the present time top performance sailplanes are becoming so clean that about the only remaining avenue for further performance improvement is to obtain more laminar flow, as a laminar boundary layer has considerably lower drag than a turbulent layer. **Figure 1** clearly indicates the gains that are possible. The figure applies only to a flat plate but for thinner airfoils the relative gains are about the same. The ordinate is $2C_f$ to account for both sides of the plate and as you can see, the values are comparable to those for an airfoil. At R_c equal to two million which would be representative of a sailplane we see that if there were only 10% laminar flow the drag coefficient, i.e. $2C_f$, would be about 0.0075. If the surface had the proper shape and smoothness to obtain laminar flow to 50% chord the coefficient would fall to about 0.0052. But if some system were devised for obtaining 100% laminar flow the coefficient would fall to 0.0019. Thus drag reductions are the incentives behind efforts at laminar flow control. Natural laminar flow cannot exist beyond an x-Reynolds number $-Ux/\nu$ of about 3×10^6. For that reason the last part of the $R_c = 4\times10^6$ curve is shown dashed, but the solid portions definitely can exist.

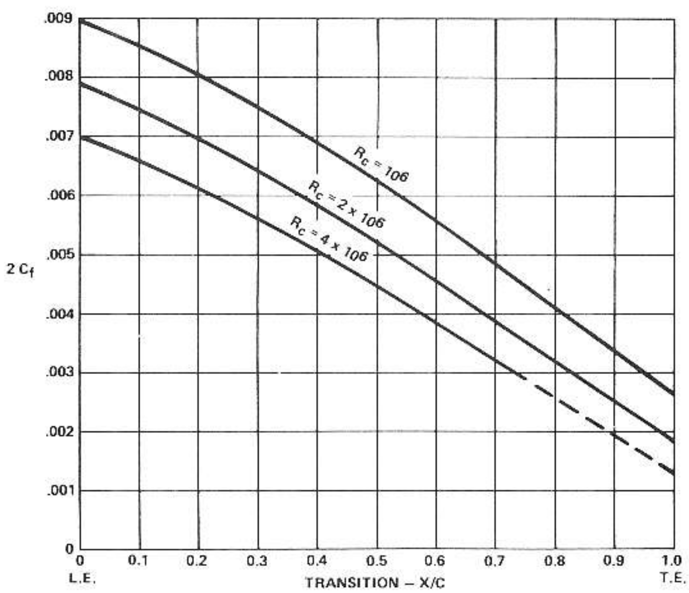


Figure 1.
Flat Plate Drag vs. Transition Point.

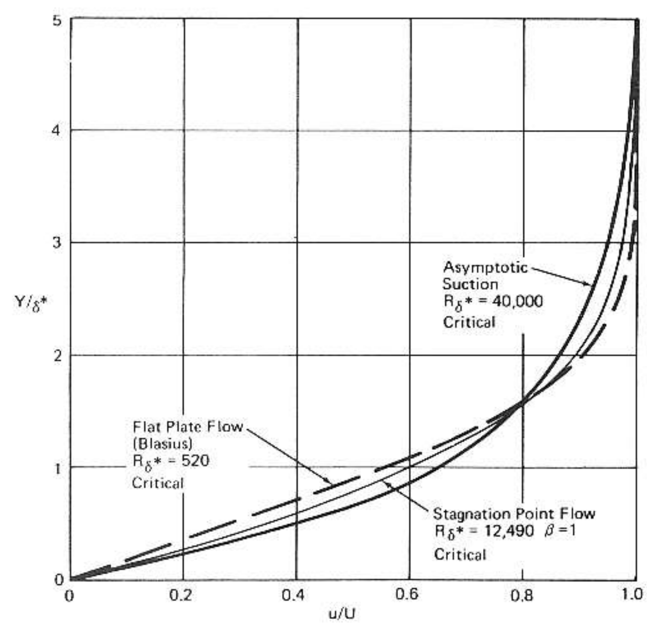


Figure 2.
Effect of Suction and Pressure Gradient on the Shapes of Boundary-Layer Profiles.

HOW CONVENTIONAL LAMINAR FLOW CONTROL WORKS

Conventional laminar flow control, hereafter called LFC, is done by sucking off some of the boundary layer though the surface. In all theoretical analyses a very idealized uniformly porous surface is assumed, through which there is a small amount of inflow to the inside of a wing. The inflow velocity is quite small but it changes the stability of the boundary layer for the better, even in adverse pressure gradients such as occur at the rear of an airfoil. Of course also the boundary layer is thinned somewhat but the change in its shape and hence its stability is the big change.

Figure 2 shows something of the effect of suction. In this figure δ^* is the boundary layer displacement thickness which is a convenient measure of the thickness of the boundary layer. It is ¼ or ⅕ of the total thickness. In fact in the figure the ordinate is y/δ^* so the value 1.0 is also just δ^*. $R\delta^*$ is a Reynolds number based on this thickness. The Blasius case is just the flat plate case. If the flat plate has weak uniform suction but still no pressure gradient the shape of the boundary layer profile becomes fuller, that is, more convex, and this slight increase in convexity raises the Tollmien-Schlichting type of stability all the way from 520 to 40,000. Also shown is the boundary layer profile that exists at the front stagnation point (Hartree $\beta = 1$) where velocities are increasing rapidly. As is seen, a favorable pressure gradient gives stability too, as is well known. So, while much more can be said about this problem the figure shows that uniform suction (the asymptotic solution) greatly stabilizes the boundary layer, as do favorable pressure gradients. Furthermore in very weak adverse pressure gradients the stability of the flow along a solid plate falls to an R^* value under one hundred and promptly turns turbulent, if it does not separate first. But weak suction in adverse gradients still stabilizes it.

The calculations in Figure 2 are for a mathematically ideal porous surface. The practical problems of producing such a surface are tremendous, so an extensively used substitute is discrete slots, spaced two or three per cent chord apart. This substitute method is not as good ideally as the porous surface but at least it has successfully been built a number of times. **Figure 3** taken from Ref. 1 shows what a slot does. Like the asymptotic suction it makes the boundary layer profile fuller (more convex) and so more stable but shortly thereafter the profile reverts to that of a flat plate. But in practice this method has been found to be quite effective. The term v_0 is the flow normal to the plate at the plate.

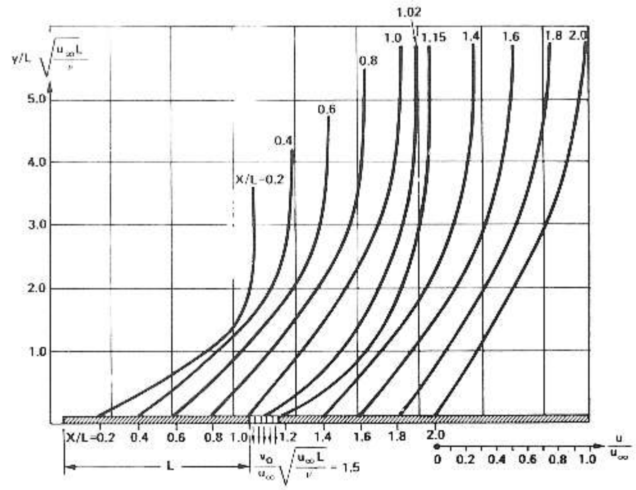


Figure 3.
Figure 8, p. 787 of Ref. 1—Several Calculated Velocity Profiles for the Plane Plate with Suction. $v_0^* = 1.5$ in the Range $1.0 \leq x \leq 1.15$. (L is used as length of the plate in this figure.)

Pfenninger is the pioneer in efforts at LFC. He has always used slots. One example of his work is shown in **Figures 4, 5** and **6** copies from Ref. 2. Figure 4 shows his 10.5% thick airfoil, and the location of the slots as well as their width which varied between 0.004 and 0.016 inches on a model of 40 inch chord. These spacings and widths should be about right for a sailplane except that if a proper shaped airfoil is

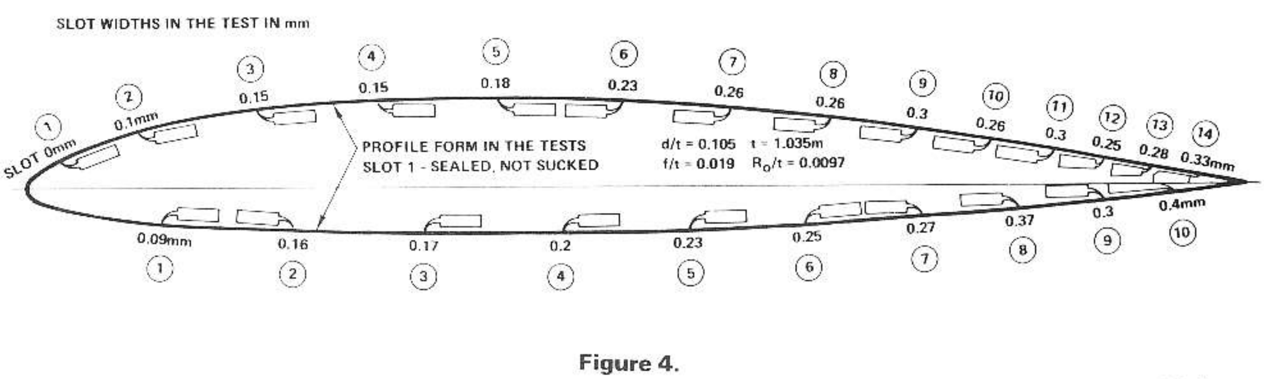


Figure 4.
Pfenninger's Figure 94—Laminar-Suction Profile, d/t=0.105, f/t=0.019. Profile Shape, Shape and Position of the Suction Slots. (In his figures d is thickness, t is chord and f is camber.)

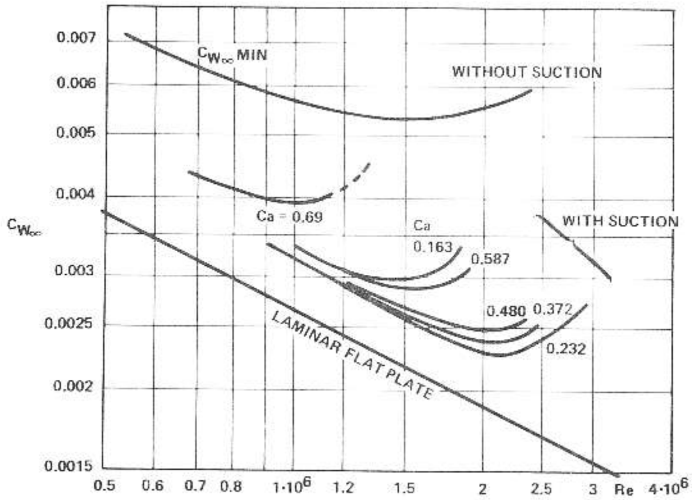


Figure 5.
Pfenninger's Figure 95—Laminar-Suction Profile, d/t=0.105. Optimum total $C_{W\infty}$ with suction (suction-blower power included) for various R_e and C_a, furthermore, $C_{W\infty min}$ without suction. (In his figure C_a is lift coefficient and C_W is drag coefficient.)

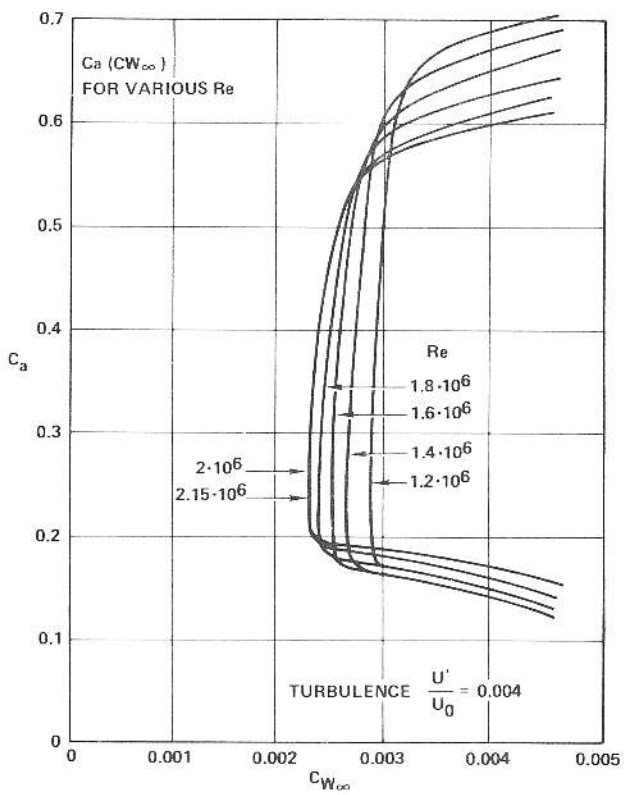


Figure 6.
Pfenninger's Figure 96—Laminar Suction Profile, d/t=0.105. $C_a(C_{W\infty})$ with Suction for Various R_e (suction-blower power included.)

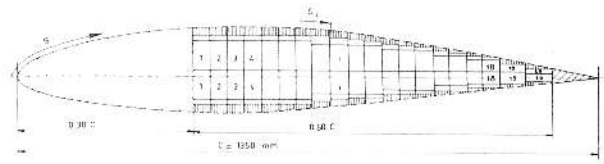
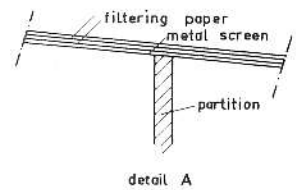


FIG. 11.2 CROSS SECTION OF MODEL SHOWING SUCTION COMPARTMENTS



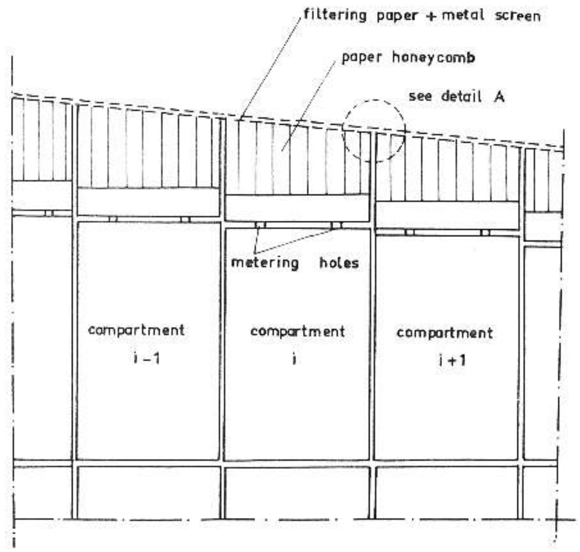


FIG. 11. 3 : CONSTRUCTION OF THE POROUS SURFACE

Figure 7.
van Ingen's Figure 11.2 and 11.3—Cross Section of Model Showing Suction Compartments.

used no slots should be needed on the front half. See **Figure 7.** Figure 5 shows measured drag values including the pumping power at various lift coefficients. Figure 6 shows the data in polar form. The drag is indeed seen to be very low. Note that the test Reynolds numbers are about the same as for a sailplane.

Braslow at NACA successfully tested a three-foot chord airfoil model covered with sintered porous bronze Oilite sheet; it too was successful. More recently J.L. van Ingen[3] as his doctoral thesis studied and tested a 15% thick airfoil that had a permeable suction surface. The airfoil and suction area are shown in Figure 7 which shows the number of duct compartments (the very inside). **Figure 8** is a view of the airfoil without its exterior covering. It is quite complicated, perhaps mainly because it was a research model. The suction surface was built up of screen wire on honeycomb to support filter paper to support fine nylon. This was not a practical surface but it too was successful in the wind tunnel, R_c values being in the 3 to 5×10^6 range. Many more tests have been made than the three mentioned, and these few short paragraphs are only meant to convey a rough picture of the situation.

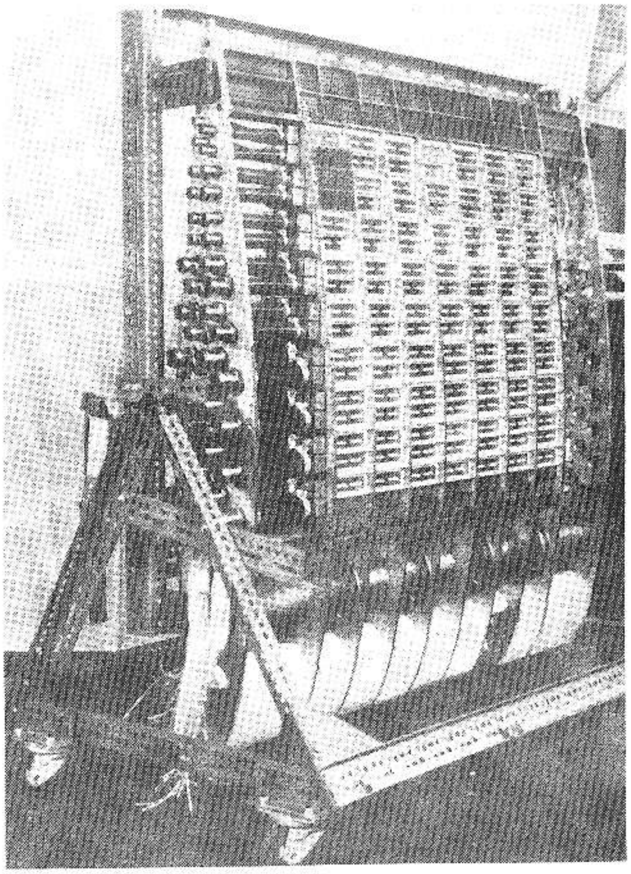


Figure 8
van Ingen's Figure 11.4—Interior of Model.

THE LFC SYSTEM FOR A GLIDER

Figure 9 shows this author's concept of the LFC system that must be used for a glider. Pumping of the aspirated air must be done and this takes power. Since a glider cannot depend on auxiliary power except for instruments the only efficient solution is to incorporate a windmill or air turbine. Because Reynolds numbers are low the airfoil can be shaped to generate a natural laminar flow for the first 50% chord or more. Aft of this point suction must then pick up and extend the laminar flow to the trailing edge just as van Ingen did. The suction will usually be from both sides of the wing. The aspirated air now is in the interior of the wing. Collecting the air in the wing is a real problem. If the natural interior of the wing is used as a duct, the duct will be large but have very rough walls. Or are there to be many tributary ducts all leading into one main duct in which case they will be smooth but smaller? Much study and thought is needed on this problem. In all existing airplane studies the LFC system uses hundreds of small ducts leading into larger ones that in turn lead into still larger ones, like a large river system.

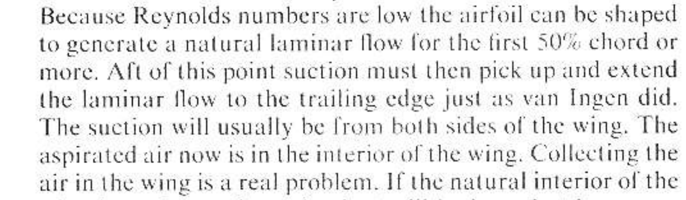


Figure 9
The Glider LFC System

For any ducting system the air that is sucked in must be led to a pump. Probably there will be one pump for each wing although a single central one could be installed in the fuselage. If there are two pumps they will likely be placed near the center of area for each wing. Then the inboard air flows outboard to the pump and the outboard air flows inboard, thus permitting two ducts instead of one per wing if the air were all ducted into the fuselage. The pump is shown schematically in Figure 9.

Also shown is the windmill. It proves to be quite small which was a pleasant surprise—about a foot diameter on a glider the size of the *Nimbus* 3. The horsepower required is substantial, but the reason the windmill can be small is that it is essentially operating in a gale. For a ground windmill 20 knots would be a stiff breeze. But this one is operating at upward of 50 knots. The windmill can probably have fixed pitch and will work satisfactorily at all speeds. For laminar flow control, as the glider speeds up, more air must be aspirated. But as it speeds up the windmill will run faster, thus speeding up the pump so that everything works together. More exactly, in the detailed analysis airspeed cancels out.

In the sketch, Figure 9, the windmill is shown removed from the pump. This was done to facilitate the analysis, and of course many sorts of drives are possible. But clearly what seems simplest and lightest is to mount the pump (just a good fan) on the other end of the windmill shaft. Then gears, belts, etc. would not be needed. In this case the simplest procedure is to exhaust the LFC air concentric with the windmill shaft. The LFC air may be exhausted at less than flight speed so that the windmill might be running in air that has deficient energy. The difficulty can be overcome, however; the diameter of the windmill just needs to be increased. In fact there is a similar problem on torpedoes. They have a propeller at the very tail that operates in the low speed wake from the entire torpedo. The propeller is especially designed to operate in this area of low energy flow. In fact it is called a "wake adapted propeller". What is needed here is a "wake adapted windmill".

A windmill is simple but not very elegant. It is also possible to mount a turbine in some kind of duct running through the wing, or in a nacelle. All these alternates imply much study. Of course if the turbine were entirely submerged in the wing structural problems will arise.

In the glider LFC system three types of wakes that form the drag are shed, see Figure 9. The usual one is the wing wake. It is conventional but quite small if the entire flow is indeed laminar. Next is the windmill wake. The windmill takes energy out of the air, which means slowing up of some streamlines so that another wake is developed. Thirdly the LFC air is discharged. Depending on the power put into the pump the air may by discharged at anywhere from much less than flight velocity to much more, in which case there would be some thrust just as with jet engines. If the discharge velocity is low the required windmill power will be low and hence its drag will be low. Conversely if the discharge velocity is high the windmill drag will be high. Hence there is an optimum velocity that makes the windmill drag and the LFC air drag together a minimum. The optimum discharge velocity is a very important and quite realistic case, because then

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there is no momentum reaction at all from the LFC air. In the Appendix, calculations are made of optimizing the LFC air discharge velocity, drag of the system, etc.

Required suction inflow velocities are very low, a value of $v_0/U\infty$ of 0.0008 being typical. Here v_0 is the average velocity through the surface, usually ratioed to flight velocity. Thus if flight is at 100 fps the seepage flow is only 0.08 ft/sec. The pressure that the pump must overcome corresponds to a pressure coefficient of about C=-1.5. The aspirated air is sucked from the very bottom of the boundary layer, where it has lost all of its dynamic head, i.e., a total head loss of C_p=-1. But the flow is being pulled from a negative pressure region typically having a C_p=-0.4. In addition, there are pressure losses through the porous surface as well as the duct losses. So a good round number is to assume the LFC air has lost a head corresponding to C_p=-1.5. One interesting fact is that the pressure within the collecting duct must everywhere be less than the external pressure. The pressure on the wing surface varies greatly. If the internal pressure were just an average of the external values, the flow would enter the wing at one place and flow out at another. For example look at Figure 9 near the dashed line marking the beginning of suction. On typical airfoils there will be considerable vacuum at this place. But near the trailing edge the vacuum will be much less. Then unless all duct pressures are sufficiently low there could be outflow near the front of the suction material. Outflow is catastrophic to the laminar boundary layer; it destabilizes the boundary layer, working just the opposite of suction.

AN ESTIMATE OF THE GAINS IN PERFORMANCE THAT MIGHT BE EXPECTED

The 64 dollar question is what LFC does to glider performance, and below is an estimate. The estimate is meant to be conservative, not an all out application of LFC. Yet in a way this estimate is an upper limit, for it assumes 100% laminar flow on both surfaces of the wing. Gaps as at ailerons and flaps are a very bad problem but laminar flow has indeed been obtained past flap gaps by using extra suction. Of course when a final practical design is being considered, decisions might be made to seek only 80% on top and 85% laminar flow on the bottom, for instance. The present purpose is to show possibilities that have some chance of being realized. After all it is indeed entirely possible to make a glider that has no flaps at all and the ailerons can be relatively small. Moreover the airfoil shape may be different from conventional, and flap settings, if any, are quite unknown for this study.

Therefore what seems to be a reasonable approach to determining the benefits is conceptually to put a new wing on the *Nimbus* 3 or AS-W 22. Any LFC sailplane will be quite expensive and naturally will be of the highest quality. Since the above two gliders represent the state of the art and are very clean, a logical procedure then is to make corrections to them as a base. The procedure below reduces to one of estimating the reduction in drag due to changing to an LFC wing.

There seemed to be no better or more accurate method for establishing a base than to convert Richard Johnson's flight test data[4,5], to C_L-C_D polar form. Some of his results as well as the predictions of Streather[6] are shown in **Figure 10**. Two flap settings were selected to represent a variety of conditions. When put in coefficient form the *Nimbus* 3 zero degree flap data looks too good to be true at high lift coefficients, for the drag increases with C_L at a rate even less than the pure induced drag—$C_L^2/\pi AR$—for an aspect ratio 36

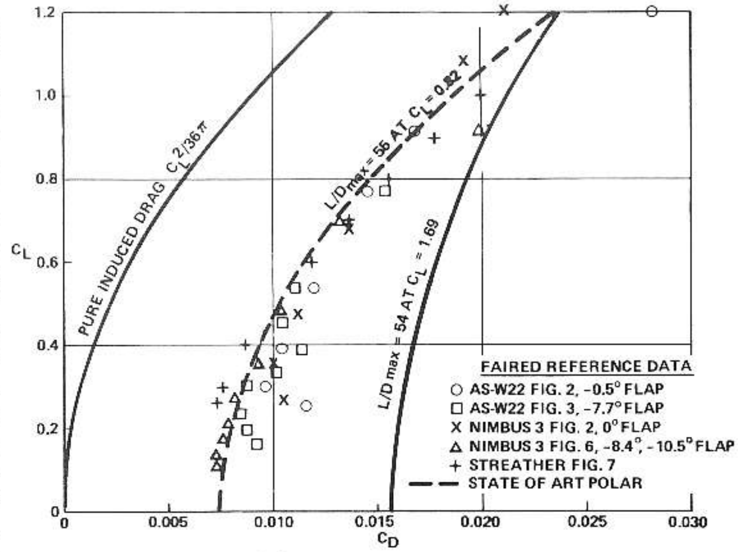

Figure 10.
Some test data for existing sailplanes together with estimated polars for sailplanes with LFC wings. L/Dmax values for several polars are noted. Aspect ratio is assumed to be 36.

wing. Perhaps there was a slight upcurrent the day these flight tests were made.

In trying to use the test data as a base a curve meant to be a mean was faired through the data. It looked parabolic in shape, so it was checked by plotting C_D vs. C_L^2 which is a straight line if the curve is indeed a parabola. It was found that this "eyeball" curve was indeed quite straight so the author decided to use this widely used parabolic polar approximation. The equation was found to be

$$C_D=0.0074+0.0110\,C_L^{\,2} \qquad (1)$$

giving e, the span efficiency factor, a value of 0.80 for an aspect ratio of 36 representative of the *Nimbus* 3 and AS-W 22. Clearly the parasite drag coefficient C_{Dp} is 0.0074. The polar is the "state of the art polar" plotted in Figure 10. At both low C_L and high C_L values it is conservative. In the mid C_L range it looks optimistic. But note that none of the plotted test data are for any positive flap settings. Therefore it is believed that eq. (1) is a good representation of the state of the art for all flap settings. It produces a maximum lift-drag ratio of 55. The polar of course is not meant to represent just one configuration. It is meant to represent an envelope of what can be obtained by use of the best airfoil and best flap settings for conventional airfoils. Streather's predictions[6] were included because it was considered valuable to see what others thought could be done. But his data points are not test data.

The next problem is substantially to correct this polar for a change in airfoils. The author does not know exactly the airfoils used by these sailplanes but one important airfoil is the FX 67-K-150/17. Based on average chord the Reynolds number of the *Nimbus* 3 at 60 kt at sea level is 1.43 million. Hence we shall work with 1.5×10^6. Different Reynolds numbers should indeed be used for the different C_L values on the polar, but since nothing is known about the airfoil shape, the best that can be done is to estimate data for a representative Reynolds number which is taken to be 1.5×10^6.

At this condition for the Wortmann airfoil a typical C_D value is 0.0065. This value was checked against information in Abbott and von Doenhoff's book[7] which shows drag coefficients for several airfoils against Reynolds number. An airfoil that is close to glider sections is the NACA 65_3-418. At $R_c = 1.5 \times 10^6$ its minimum drag which occurs at a design lift coefficient of 0.4 is 0.0068. This is an 18% thick section. The complete *Nimbus* 3 wing has a mean airfoil thickness less than 15%, being 15% at the root. Using Figure 68d of Ref. 7 to correct for thickness we again obtain $C_D = 0.0065$. The average airfoil thickness is less than this. Furthermore assuming a low value for C_D for the base airfoil is conservative because the incremental improvement will be less. Therefore for the base airfoil assume $C_D = 0.0064$.

For estimating the airfoil drag with laminar flow Pfenninger's NACA TM 1181[2] is used. His work considers the pumping problem for an airplane as if engines existed so his total or equivalent drag value cannot be used directly. Instead, as in Figure 9, consider the wake drag, the windmill drag and the discharge drag. From Figure 97 of the TM 1181 read C_D wake $= 0.00075$ at a flow rate that leads to minimum total drag. This figure is for a Reynolds number of 2.11×10^6. Using this Figure 95 (our Figure 5) as a guide, estimate that for $R_c = 1.5 \times 10^6$ the drag would have increased by 0.00025 giving a wake drag coefficient of 0.0010. This is the value that shall be used.

Examination of various data such as Pfenninger's and van Ingen's indicates that an average normal inflow velocity ratio v_0/U_∞ of 0.0008 is representative. In addition it is estimated that the airfoil is so shaped that suction need begin at 50% chord on top and 60% chord on the lower surface. Then the total inflow quantity will be, with c as the chord

$$Q = 0.0008 U_\infty c(.4 + .5)$$

A standard measure of suction quantity is

$$C_Q = Q/S\,U_\infty \text{ or } Q/c\,U_\infty$$

depending on whether the entire wing area is being considered, or just a wing section of chord c. Then for the present problem $C_Q = Q/c\,U_\infty = 0.00072$ is obtained.

In the appendix an equation is derived for the minimum drag of the discharge wake plus windmill wake. But partly because a glider cannot always operate at optimum conditions and partly because of simplicity we use a different formula for the pumping drag. We use one for the case where the aspirated air is discharged exactly at a flight velocity, so the only drag of the pumping system is the drag of the windmill. This kind of operation leads to about 10% more pumping drag than the true minimum. The formula is

$$C_{D_{pumping}} = \frac{C_Q(1 - C_{p2})}{\eta(1-a)} \qquad (2)$$

Here η is the product of the windmill efficiency and the fan efficiency. Assume it to be 0.70. Then if both fan and pump had the same efficiencies, each would be 84%. The term a is a measure of the disk loading of the windmill, see the Appendix. A value of 0.2 seems reasonable and practical. Hence $\eta(1-a) = 0.56$. C_{p2} is the static pressure coefficient at Station 2, Figure 9, the entrance to the fan. As noted before assume it is 0.5 below static, so $C_{p2} = -0.5$. Then obtain from eq. (1)

$$C_{D_{pumping}} = 0.00072\left(\frac{1 + 0.5}{0.56}\right) = 0.0019$$

This is the total pumping drag. It is important to have high efficiency. If η were 0.60 instead of 0.70 the pumping drag would increase four counts to 0.0023.

Then adding the airfoil wake drag we get for the total LFC airfoil drag

$$C_D = 0.0019 + 0.0010 = 0.0029$$

Notice that the pumping drag is considerably greater than the wake drag. While arrived at differently 0.0029 is practically the same value as Pfenninger measured, and shown in Figure 5 for the proper Reynolds number. For the plain airfoil the estimated drag value was $C_D = 0.0064$. Hence a reduction is made of

$$C_D = 0.0064 - 0.0029 = .0035$$

Note that the tail drag, fuselage drag, interference drag and induced drag are left unchanged. On an LFC airfoil only the skin friction is changed, not moment coefficients, etc., so there is no reason to change tail areas and the like. Then for the corrected polar $C_D = 0.0035$ is subtracted at all C_L values. This is an envelope treatment, because for any particular airfoil design the increment may not be attainable at certain C_L values. But using a different airfoil the increment should indeed be attainable. At high Reynolds numbers as for airplanes the increment would be much greater, but even at $R_c = 1.5 \times 10^6$ it is still substantial. The new polar is

$$C_D = 0.0039 + 0.0110\,C_L^2 \qquad (3)$$

It is also shown in Figure 10. The maximum lift-drag ratio is noted and is increased from 55 to 76. For the state of the art polar maximum L/D occurs at about $C_L = 0.82$. For the LFC wing it occurs at $C_L = 0.59$ so the speed for max L/D would be higher meaning both shallower glides and faster cruising speeds. Again remember that weights, wing areas, aspect ratios and residual drag values have been assumed to be unchanged. No doubt the weight would be increased by all the complications of the LFC installation.

The pumping drag was seen to be greater than the wake drag. While not legitimate for a glider I looked into the problem of running the pump by means of solar cells built into the wing surface. There is enough wing area to accommodate the necessary quantity of solar cells. In this case assume that there is enough power to discharge the LFC air at U_∞ so there will be no pumping drag at all. The power required is not much greater than for the optimum windmill case. Now the only drag is the wake drag which has the value 0.0010. The polar for this case is also shown in Figure 10 and has the equation

$$C_D = 0.0020 + 0.0110\,C_L^2 \qquad (4)$$

Maximum L/D has increased to 106. The lift coefficient for it has now reduced to about 0.43, so cruising speeds can be still faster. This configuration is not presently legal, but it was included to show what LFC can do. In fact one could cheat in this case. Using more solar cells in especially bright conditions, excess power could be available so that the LFC air could be discharged at velocities greater than U_∞. Then the glider would have a small amount of jet propulsion. Of course a sailplane with both LFC and solar cells would be a terribly complicated and expensive device.

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THE GRIFFITH AIRFOIL— A POSSIBLE ALTERNATIVE

In conventional LFC the airfoil is usually a conventional shape as in Figure 4, and the surface is simply made permeable over an extended area. If suction is turned off the airfoil properties become little different from normal airfoil properties as can be partially seen by examination of Figure 5. Especially for a sailplane, how to make the suction surface is a formidable and unsolved problem. In fact this hardware problem is probably the greatest obstacle to its application. The possible surfaces go all the way from van Ingen's wind tunnel model, through porous plastics, uniformly distributed perforations, fine screen wire, through bands of perforations that correspond to slots, through slots themselves. Slots, as I have already mentioned, are quite small. An interesting current and so far successful development is by Douglas Aircraft Co. for LFC on large transports. They perforate 0.25" thick titanium sheet by means of an electron beam that creates tiny 0.002" diameter holes spaced only 0.035 inches apart. Since a glider operates at a lower Reynolds number per foot the hole spacing and diameter would not have to be quite so small, but at least this information gives readers some idea of the construction problems and complexity of LFC. August Raspet did an interesting LFC experiment. His glider was covered with doped fabric. He just perforated it by moving a sewing machine mechanism and needle along the wing. The operation was successful.

Because of the formidable fabrication problem I looked into the Griffith airfoil as a possible solution. The Griffith airfoil was a remarkable concept that was mainly investigated in England around World War II time. A.A. Griffith said that if suction was to be used, all the suction and all the adverse pressure gradients should be concentrated at the same place. The suggestion has tremendous implications; it implies actual pressure jumps. Considerable work on the idea was done in Great Britain and it is summarized in a Wright Bros. lecture by Sidney Goldstein[8]. One of the earliest designs that was tested is shown in **Figure 11**, a 16.2% thick airfoil shape. We use it to exhibit the major features of these airfoils. In this case there is a discontinuity in velocity

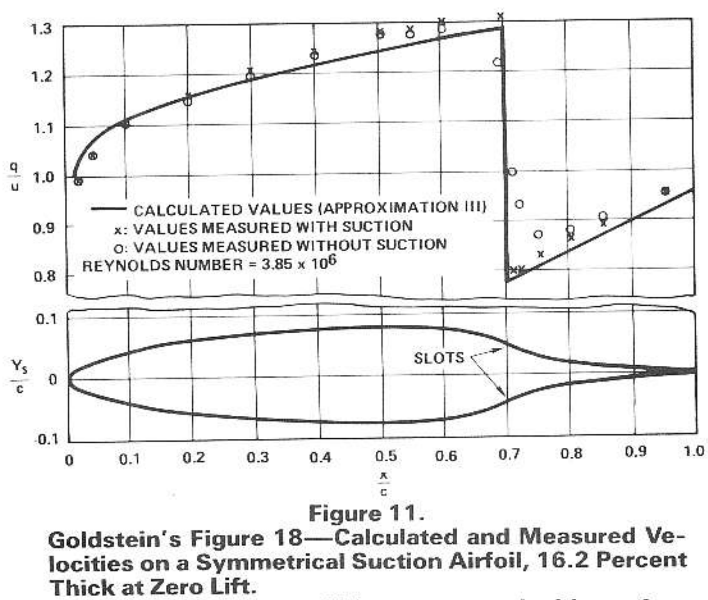

Figure 11.
Goldstein's Figure 18—Calculated and Measured Velocities on a Symmetrical Suction Airfoil, 16.2 Percent Thick at Zero Lift.
Calculated Values. xxx: Values measured with suction. ooo: Values measured without suction. Reynolds number—3.85 × 10^6. (In these figures q/U is local velocity ratio, and Approximation III is one of their methods.)

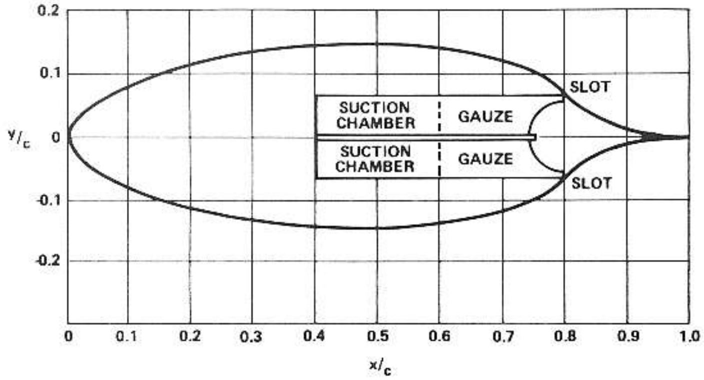

Figure 12.
Goldstein's Figure 12—Symmetrical Suction Airfoil 30% Thick.

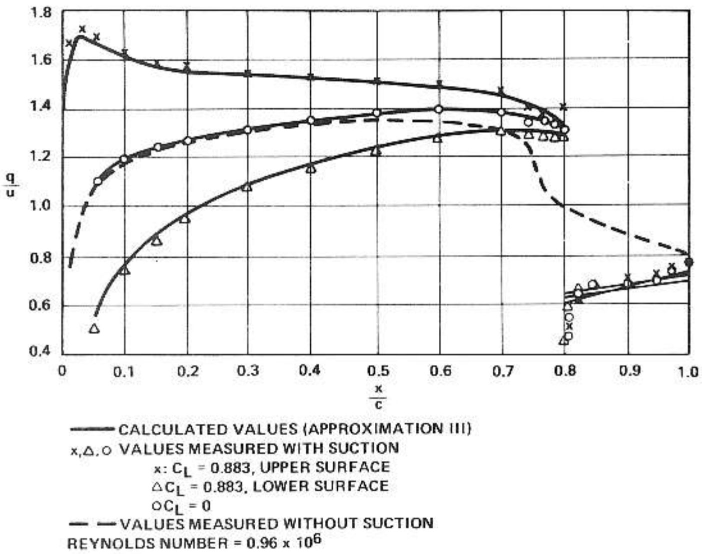

Figure 13.
Goldstein's Figure 19—Calculated and Measured Velocities on a Symmetrical Suction Airfoil, 30 Percent Thick (Figure 12), at $C_L = 0$ and $C_L = 0.883$.

and pressure at 70% chord. At this discontinuity the suction necessary to prevent separation is applied. The amount of suction needed is very easily and accurately calculated by a criterion given by Sir G. I. Taylor. With all the adverse gradients concentrated in one place it is now possible to have favorable gradients over all the rest of the airfoil, so at least near design conditions there is no danger of separation or laminar bubbles. Obtaining extensive laminar flow means that the boundary layer must be naturally laminar to the slot. This is a serious defect of these shapes for large airplanes. But for a glider whose Reynolds number is under 3 million this is no problem for a good quality surface.

Originally the British expected to get laminar flow behind the slot also because the pressure gradient is favorable there too. But to develop the pressure jump the rear surface must be concave. This causes Görtler type of boundary layer instability and turbulence behind the slot. But the data that will be used later are realistic because they are all for transition at the slot. In fact this feature ends up as a possible practical advantage. The portion aft of the slot can be made movable and there is no worry about trying to get laminar flow across a flap or aileron gap. The suction slot itself is of the order of 1/32 to 3/32 inch wide. Also it is interesting in Figure 11 that the flow does not seem to be altered drastically with suction off. On a glider since there is no power in the first place suction failures are unlikely unless a bearing froze or something broke.

Now some further Griffith airfoil possibilities will be shown but again, it is emphasized that the essential features of the Griffith airfoil are: (1) suction concentrated at a point; (2) adverse pressure gradients all concentrated at this same point; (3) the rest of the airfoil at the design condition may then have entirely favorable pressure gradients. These airfoils can be made very thick. **Figure 12** shows a 30% thick symmetrical Griffith airfoil. This airfoil was wind tunnel tested and the measured pressure distributions were in good agreement with the theoretical, **Figure 13**. Improved theoretical design methods now existing could eliminate the adverse pressure gradients starting 10% or so ahead of the slot at $C_L=0$. Other data show that except for this fault the pressure gradient is favorable up to $C_L=0.4$ meaning that the laminar bucket should be between $C_L=\pm 0.4$ or 0.8 wide. This airfoil has been tested in the wind tunnel at angles of attack up to 15° and with flap deflections up to 14°. All the coefficients were well behaved and C_L max was found to be 2.47. The figure shows how suction and ducting were handled for the wind tunnel model. The slots formed the flap gap.

Because the idea was new the British did their original work around symmetrical sections as in Figure 11 and 12. But the sections certainly do not need to be symmetrical. **Figure 14** shows a single slot shape 41.2% thick. Furthermore it has no adverse pressure gradients between $C_L=0$ and $C_L=2.85$, that is, the laminar bucket is a C_L of 2.85 wide. It is so wide because the airfoil is so thick. **Figure 15** shows a single slot shape 31.5% thick, that has been tested, built and flown as an experimental wing on a small cargo glider. It was none too successful, the Reynolds numbers being high so that laminar flow did not reach the slot. Furthermore the slot was not designed correctly so the airfoil did not perform up to theory. But while the glider was in flight test, further work, modifications, and tests brought it in line with the other results. The primary goal in this work was to obtain test data for a prototype of a large transport. For that goal the effort was a failure, because Mach numbers and Reynolds numbers were too high.

But the airfoils seem especially suitable for a glider. Very thick sections are very poor at high Mach number as for jet airplanes, but sailplanes operate at very low Mach numbers. Furthermore for these airfoils to work, laminar flow must exist naturally in the slot. Sailplane Reynolds numbers are low, and fully favorable pressure gradients exist near the design condition so that laminar flow certainly should exist. In fact even for flat plate flow, where there is no favorable pressure gradient at all, the natural laminar run is about three million. Also being nicely convex the shape seems easy to construct to high quality. As seen, tests have been run with suction off. The results indicate that a wing would be controllable laterally, but of course with less aileron effectiveness. Theory shows that the slope of the C_L vs α curve becomes steeper for thicker sections. That is indeed the way these showed up in tests; they agree with theory. Quite a bit more work has been done than mentioned here. Even one section 70% thick was designed. This somewhat lengthy discussion of Griffith airfoils has been given because it is suspected than many today do not know of this work at all. In order to learn more see Goldstein's Wright Brothers Lecture (Ref. 8).

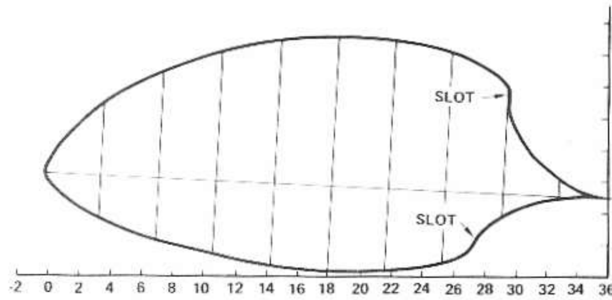

Figure 14.
Goldstein's Figure 14—Cambered Suction Airfoil 41.2 Per Cent Thick with a C_L-Range from 0 to 2.85 for the laminar bucket.

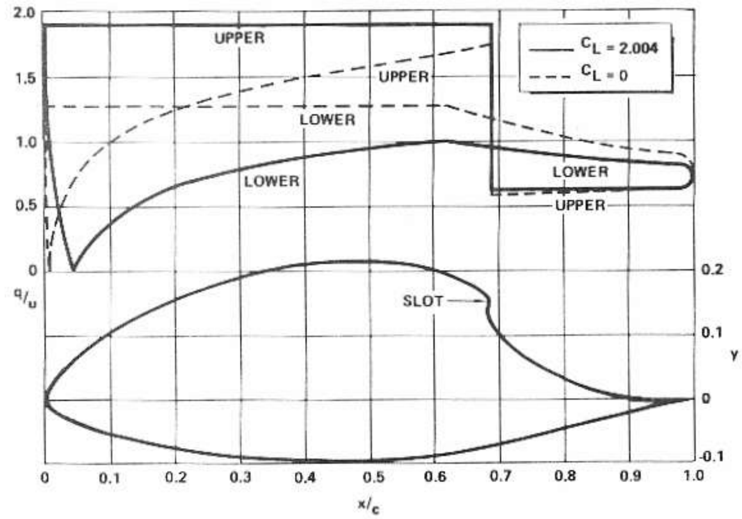

Figure 15.
The GLASW II Airfoil 31.5% Thick. Velocity distributions are shown for $C_L=0$ and 2.004. Within this range there are no adverse velocity gradients in forward regions so the laminar bucket theoretically extends from $C_L=0$ to 2.004.

Perhaps the reader has not yet realized that the Griffith and conventional LFC airfoils are quite different in principle. The conventional LFC airfoil is sufficiently thin and fair that the flow will not separate with suction off. The suction changes the boundary layer profile shape as in Figure 2 making it much more stable. The Griffith concept is basically a separation control idea. Suction is applied at the pressure jump to prevent separation. Then because separation is prevented the designer is free to make radical airfoils. The laminar flow that exists to the slot is what is called natural laminar flow created by favorable pressure gradients, just as for the front parts of ordinary airfoils, only more so. But with separation prevented very favorable gradients can be developed and glider Reynolds numbers are so small that transition should not occur ahead of the slot. Also glider designers are free to use greater thickness to create still more favorable gradients if desired because Mach number is no problem.

ESTIMATES OF PERFORMANCE USING A GRIFFITH WING

There is a moderate amount of test data on the symmetrical 30% thick Griffith airfoil so we shall make our estimates around it. Estimates shall be made in two ways that help set

(Continued on next page)

bounds to the performance. Ref. 8 presents a chart for the 30% thick airfoil that shows the ideal effective drag coefficient that includes pumping power. This chart essentially is for 100% efficient pumps and no losses in the ducts. Hence it is optimistic but it is nevertheless interesting to know the upper bounds to the performance possibilities.

Fortunately enough data were obtained in wind tunnel tests to apply eq. 2 just as was done for the LFC case. Then while not exactly a lower bound it at least provides a far more realistic assessment of these airfoils.

Our first estimate is to replace the *Nimbus* 3 or AS-W 22 wing by the 30% thick Griffith, using Goldstein's chart for ideal effective drag coefficients (his Figure 25). While not a very practical replacement wing we cannot be very choosey because of the lack of data. Since the planform is unchanged the reference Reynolds number will be 1.5×10^6 as before. At this Reynolds number Goldstein's Figure 25 indicates C_D to be 0.0036. Now do as done for the LFC case, subtract a drag increment from the state of the art polar. At $R_c = 1.5 \times 10^6$ C_D of the base airfoil is 0.0064 and for the 30% Griffith it is 0.0036 so the increment is 0.0028. For the LFC case it was 0.0035. This value is subtracted at all C_L values; not enough is known about the airfoil design or what would be used in an actual glider to do any better. The result is shown in Figure 17 and the polar equation is

$$C_D = 0.0046 + 0.0110\,C_L^2 \qquad (5)$$

Then L/D max computes to be 70, but of course if a more reasonable thickness were used the performance would improve. The drag data assumes turbulent flow behind the slots.

The above case does not use the advantage of extra thickness. Now take advantage of it in an extreme way, which is not necessarily a practical way, but is at least illustrative of possibilities. Simply halve the chord of the wing, meaning doubling the aspect ratio to 72—a real rapier wing. This is possible because the same root spar depth as for the base case is retained. The fact is clearly indicated by **Figure 16**. Incidentally in these estimates for the Griffith airfoil it is assumed that the 30% thick wing exists along the entire span.

In actuality only the root need be 30% thick, so that the average thickness can be substantially less, a benefit not considered here. In fact for a 0.3 taper wing having a 30% root section and a 12% tip the average thickness is 26%.

In this second case the planform is changed. In order to make the drag estimate the drag that is residual over and above the wing drag must be known. This must be found at low lift coefficients. At a $C_L = 0.11$ the Reynolds number is 3×10^6 and the wing drag coefficient per Wortmann's data has reduced from 0.0064 to 0.0054. Then the residual drag that accounts for the fuselage and tail, is in terms of f the equivalent flat plate area and *Nimbus* 3 wing area of 180 ft^2:

$$\text{Base polar, total } f = 0.0074 \times 180 = 1.33\ \text{ft}^2$$
$$\text{Wing } f = 0.0054 \times 180 = 0.97\ \text{ft}^2$$
$$\text{Residual } f \text{ (fuselage, tail, etc) } 0.36\ \text{ft}^2$$

The value 0.36 ft^2 seems reasonable, being about a quarter of the total drag. On the high aspect ratio design we shall assume the fuselage and tail drag are unchanged. Then using a Reynolds number of 10^6 to account for the reduced chord the new wing drag coefficient according to Goldstein's Figure 25 is 0.0043. Then the new polar is of the form

Total drag=

(New Wing Drag)+(Residual Drag)+(Induced Drag)

or with numbers and applying the residual drag to the new wing area obtain

$$\text{total } C_D = 0.0043 + \frac{0.36}{90} + C_L^2/0.80\pi(72) \qquad (6)$$

$$C_D = 0.0083 + 0.0055\,C_L^2$$

The minimum drag coefficient has increased because the drag area is now referenced to a smaller wing area. The drag has not reduced as much as first expected because the wing is at a lower Reynolds number than the others.

The polar is plotted in **Figure 17**. L/D max is 74 and the

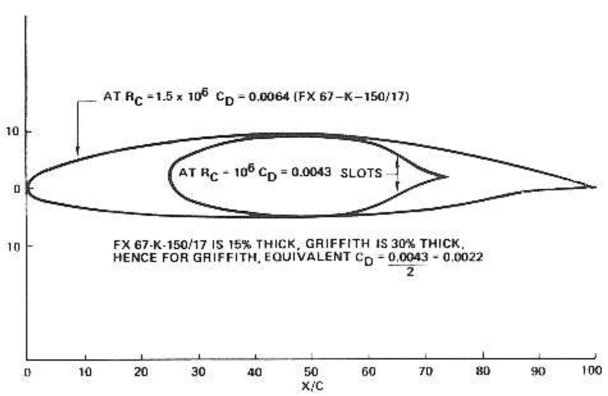

Figure 16.
A Comparison of a Thick Griffith Airfoil with a Wortmann 15 Percent Thick Airfoil at Equal Spar Depths. In the Section Drag Comparison the Griffith Airfoil Drag is the *ideal effective drag;* the Wortmann airfoil data are test data.

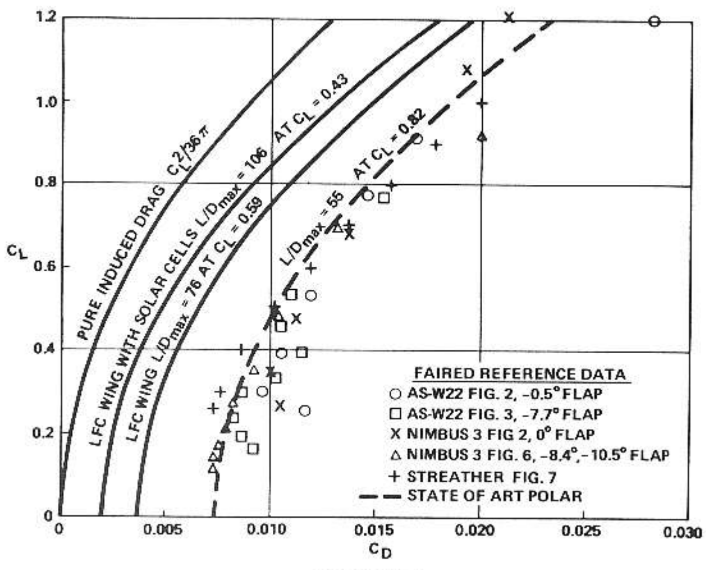

Figure 17.
Estimated polars for Two Griffith Airfoil Wing Designs. L/Dmax values are noted. The estimates are against a background of flight test data.

C_L for it is very high, 1.23. This L/D max value is not quite as good as for the LFC case but the main message is that some form of laminar flow control seems to put L/D max up in the seventies for the ideal drag case. It should be borne in mind that the various polars are just rough predictions in view of the lack of data, and the generality of the problem. Also remember that c was held the same for all cases, including the AR=72. I had no idea how to change it or even the direction for change.

The above estimates are really upper limit estimates because no losses in the induction system are considered. Therefore, reestimate the AR=72 case trying to account for efficiencies and losses by using eq. (2). To estimate the drag coefficient, values for C_p, C_Q, and C_D are needed. From Fig. 13 q/U at the slot is about 1.34 making C_p=-0.80. Then based on test data for the 16.2% airfoil estimate that C_p at pump=-0.85. From Figure 22 of Ref. 8 at $R_c = 2.88\times10^6$ read for transition at 80% chord $C_Q = 2\times0.001$. This should be corrected to $R_c = 10^6$ since this is our assumed Reynolds number for the AR=72 case. Then $C_Q = 0.002\ (2.88)^{1/2} = 0.0034$. Being laminar, Reynolds number has a considerable effect on the coefficients. Finally, from Figure 24 of Ref. 8 read the wake drag coefficient to be 0.0005. Then using eq. 2 as well as the same a and η we have

$$C_D = 0.0005 + 0.0034\left(\frac{1+0.85}{0.56}\right) = 0.0117)$$

Then as before, for the entire glider

$$C_D = 0.0117 + \frac{0.36}{90} + \frac{C_L^2}{0.8\pi(72)} = 0.0157 + 0.0055\,C_L^2 \qquad (7)$$

giving L/D max=53.7. This is worse than the original state of the art airfoil and it has all sorts of structural complications. So when an attempt is made to bring in losses and efficiencies realistically the performance goes all to pot. But more will be said about the subject in Section 10. The pumping power seems to ruin the idea.

SOME PRACTICAL CONSIDERATIONS

8.1 Duct Dimensions—Since the wing aspect ratio is so high the area available for ducting is very small. Duct velocities should be fairly low or flow losses will nullify any gains. Therefore a check of duct dimensions is certainly in order. Let us assume the duct velocity is $\frac{1}{4}U_\infty$. Furthermore assume that the pumps are in the center of area of each wing. Then the maximum flow in any duct is ¼ of the total flow. For the LFC system the C_Q assumed was 0.00072. Then for the flow rate

$$Q_{\text{per duct}} = C_Q S\ U_\infty/4 = A_{DUCT}V_{DUCT} = A_{DUCT}\ U_\infty/4$$

or

$$A_{DUCT} = C_Q S = .00072\ (180) = 0.130\ \text{ft}^2$$
$$= 18.7\ \text{in}^2 \cong 3'' \times 6''\ \text{duct}$$

Considering that a glider wing is not filled with fuel tanks and the like, it appears that room can be found for ducting even including water ballast. Uncertainty exists for the AR=72 Griffith case, because only about half as much internal space is available and C_Q is higher. Therefore, the number of pumps might have to be doubled.

Windmill and Pump Dimensions—The slightly conservative case where the LFC air is discharged at flight velocity will be analyzed. At this condition the total power that the windmills must produce is

$$\text{Power} = \frac{\rho}{2} S\, U_\infty^3\, C_Q(1-C_{p_2})/\eta \qquad (8)$$

As before η is the product of the windmill and pump efficiencies, assumed to be 0.7. Also we have assumed C_Q to be 0.00072 and C_p=-0.5 thinking of the LFC case. Then windmill power required

$$= \frac{\rho}{2} S\, U_\infty^3 (0.00072)(1.5)/0.7$$
$$= 0.00154\frac{\rho}{2} S\, U_\infty^3 \qquad (8a)$$

At sea level conditions and at 100 fps for a *Nimbus* 3 size wing the power by (8a) would be 0.6 hp or 0.3 per windmill if two are used.

The windmill power shall be determined by a simple Froude type of analysis. Such an analysis is contained in Durand Vol. 4 p 325 (Ref. 10). A formula for the power output is given as

$$\text{Windmill Power Output} = 2\pi R^2 \rho U_\infty^3 (1-a)^2 a \qquad (9)$$

Here a is a measure of the velocity through the disc of the windmill and R its radius. We have assumed a moderately loaded windmill and have chosen a=0.2. Then

$$P_{WINDMILL} = 0.8042\ R^2\, U_\infty^3 \rho$$

Then equating power required to power available we find that ρ and U_∞ cancel out, meaning that as the glider speeds up and the flow demands become greater the power available becomes greater in just the same ratio so that the windmill and pump could be rigidly attached to the same shaft.* We can solve the equation for R, the windmill radius. Note that a suitable efficiency for both components has already been introduced through η=0.7.

$$R^2 = \frac{0.00154\rho\, S\, U_\infty^3}{(2)(0.8042)\rho\, U_\infty^3} = 0.0010\ S$$

Then with S=180 ft^2 we find

$$R = 0.424 = (10.2\ \text{in dia.})$$

This is the size if only one propeller for the entire glider is to be used. If two are to be used they become

$$R = 0.300\ \text{ft} = (7.2\ \text{inches dia.})$$

(Continued on next page)

*But for some dynamic conditions, e.g., recovery from stall, it may be found that variable pitch windmill blades are advisable.

Thus the windmills are quite small. If the discharge air is along the axis of rotation the diameter will have to be increased an inch or two to get the blades in fresh air as well as further out of the wake of the wing.

The exit duct nozzle diameter is next analyzed, assuming the air is discharged at flight velocity. For a measure of the flow rate use $C_Q = 0.00072$ as for the duct dimensions. Then if we assume there are two exit nozzles we have

$$Q_{per\ nozzle} = C_Q S U_\infty = A_{nozzle} U_\infty$$

$$A_{nozzle} = \frac{0.00072(180)}{2} = 0.0648 \text{ ft.}^2$$

and nozzle diameter = 0.287 ft. = 3.45 in.

As should be obvious by the several relations the nozzle area is exactly ½ the maximum duct area.

So it is seen that the exit nozzles are relatively small. It has generally been thought that the windmills would be in the rear, probably in the duct exit wake. The reason for rear placement instead of in front of the wing, is that if in front they are likely to trip the boundary layer on the wing immediately behind them. The above analysis of some of the pumping requirements was done specifically for the LFC system but the flow rate and other requirements of the Griffith system are not greatly different.

8.3 A Possible Spoiler for the Griffith Wing—A conventional spoiler introduces problems. It indeed retracts flush with the wing but the gaps around it as well as possible leakages appear to be enough to trip the boundary layer in its vicinity. Moreover the desirable place for the spoiler is in an area where suction must be applied. On the LFC system possible remedies are to use flaps or airbrakes mounted on the fuselage. But there is a promising alternate for the Griffith wing. As already explained the Griffith wing is really a separation control system. Therefore, to increase the drag it is suggested that the suction just be turned off over part of the wing. The easiest system is just to have some kind of damper valve that would cut off the flow in the inboard portion of the ducts, so that the entire inner half of the wing is partially stalled. The cutoff might be on the upper slot, the lower slot or both. As seen from Figures 11 and 13 nothing drastic should happen. Lateral control should remain good because the outer half still has its suction, perhaps more than the normal suction because then no flow comes from the inner half. This system would be absolutely clean; in fact the spoiler system would be invisible.

A POSSIBLE HALF-WAY LFC SYSTEM

The pumping and ducting for LFC are such terrible problems that I wondered if there were any halfway measures that might be much simpler and still lead to improvement. One of the worst problems, of course, is ducting the air along the length of an extremely slender wing.

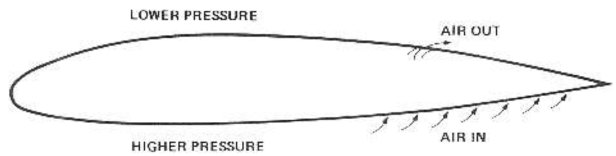

Figure 18.
A Possible System Using Natural Transpiration to Obtain Some LFC.

I wondered if it was possible to use the pressure differential due to the wing loading to drive some seepage flow through the bottom surface and then out the top side, **Figure 18.** If such a flow were indeed possible then the laminar flow regions could be increased on the bottom side. The exhaust on the top side would be turbulent but it can be in a region that is already turbulent. Moreover for reasonably thin airfoils this additional airflow should not cause separation.

To obtain a general idea as to whether something like this is possible, I applied the Douglas electron beam perforated titanium system to the glider since I know its flow properties. Let us assume a pressure differential of 6 lbs/ft^2 as is representative for a high performance sailplane. Also assume flight at 100 knots. The average pressure differential is constant, the wing loading staying constant, but local values will vary with C_L and of course the airfoil shape may have to be chosen specially to get the differential. In particular the airfoil needs to be aft loaded, but good airfoils indeed have this property.

The Douglas perforated titanium is 0.025″ thick with 0.002″ holes spaced 0.35″ apart. The holes are so small that losses vary linearly with velocity instead of with the square as for larger orifices. Results from tests show that at standard conditions with 14 lbs./sq. ft. pressure differential the average flow rate is 14.5 ft/min. The following velocity ratio can then be developed, computed as below

$$\frac{v}{U_\infty} = \frac{v}{169} = \frac{(6/14)(14.5/60)}{169} = 0.0006$$

The factor 60 converts ft/min to ft/sec. In the analysis of the LFC system we noted that the v/U_∞ needed was about 0.0008. Hence at high speed this particular perforated material is slightly deficient, but only a very slight increase in porosity would be needed to make it satisfactory. Of course any real system would require much detail study, design and test to get the necessary flow distribution. There is a good chance that hole size or spacing would have to vary over the airfoil in order to obtain the necessary distribution. But the above calculation indicates a possibility and if full laminar flow could indeed be obtained on the lower side the gains should roughly be half of those shown for the full LFC case. There will be additional wake drag due to the LFC air being exhausted out the top side, but its contribution to drag should not be any more than the equivalent pumping and of course the flow is all chordwise. No similar natural flowing system has been seen for the Griffith airfoil. However, it should be noted that by proper airfoil shaping, natural laminar flow nearly to the trailing edge on the bottom can probably be created, without the need for LFC at glider Reynolds numbers.

CONCLUDING REMARKS

In order to provide a final interpretation the predictions are converted into speed polars in **Figure 19**. All used a wing area of 180 ft^2 except for the Griffith 72 which used 90 ft^2. Flight weight was 1062 lbs in all cases. Johnson's *Nimbus* 3 flight test data are shown by the circles. As you see the Griffith ideal and LFC polars are close together and considerably better than the *Nimbus* 3, roughly being able to cruise 20 km faster at the same sinking speed. Of course the Griffith with losses is about the same as the *Nimbus* 3. The LFC plus solar cells is fascinating but of course not allowable. Approximately the minimum flying speed is indicated by the $C_L = 1.2$ line. As to be expected the Griffith 72 cannot fly as slowly.

At first glance it is surprising that the LFC and Griffith 72

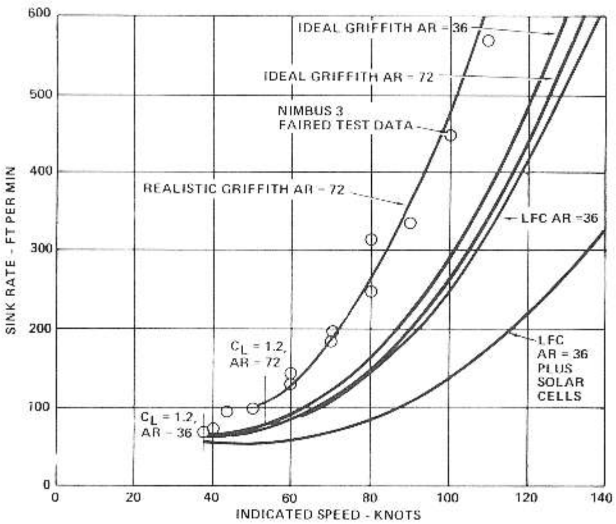

Figure 19.
Speed Polar Comparison for the Several Alternatives.

ideal polars are so near to being identical in spite of the great change in aspect ratio. But the fact is easily explained, and because the explanation is often not realized it will be given in full. Start with the widely accepted parabolic polar approximation already used

$$C_D = C_{D_p} + C_L^2/\pi e\,AR \qquad (10)$$

C_{D_p} is the so called parasite drag, the drag at zero lift. Convert (10) into drag-lift ratio by dividing by C_L.

$$\frac{C_D}{C_L} = \frac{C_{D_p}}{C_L} + \frac{C_L}{\pi e AR} \qquad (11)$$

C_{D_p} can be written as f/S, the ratio of the equivalent flat plate drag area to wing area. Also $AR = b^2/S$. Hence

$$\frac{C_D}{C_L} = \frac{D}{L} = \frac{f}{SC_L} + \frac{SC_L}{\pi e b^2} \qquad (12)$$

Next eliminate SC_L by use of the lift equation, assuming unaccelerated flight

$$L = W = \tfrac{1}{2}\rho U_\infty^2\, S\, C_L \qquad (13)$$

Then (12) becomes, remembering that $w_s/\infty = D/L$ (14)

$$\frac{C_D}{C_L} = \frac{D}{L} = \frac{w_s}{U_\infty} = \frac{\tfrac{1}{2}\rho U_\infty^2 (f)}{W} + \frac{1}{\tfrac{1}{2}\rho U_\infty^2 \pi}\left(\frac{W}{eb^2}\right)$$

In (14) aspect ratio has disappeared as a parameter. The speed is an indicated speed as might be expected and two parameters involving aerodynamics come forth, W/f and W/eb^2. These and (14) show that for low D/L f should be small and the effective span large.

Eq. 14 explains Figure 19. While the aspect ratio was doubled for the Griffith 72, W, e, and b were left unchanged so the right-hand term in (14) was unchanged, and f was not changed very much. For good L/D values a large span is needed and high aspect ratio becomes just a means to get considerable span with a minimum of area and hence parasite drag. Furthermore (14) can be differentiated to find D/L minimum and we find

$$\left(\frac{L}{D}\right)_{MAX} = \sqrt{\frac{\pi}{4}}\sqrt{\frac{eb^2}{f}} \qquad (15)$$

which again only involves span, span efficiencies and equivalent flat plate drag area.

Since $\frac{w_s}{U_\infty}$ is the drag-lift ratio we can multiply (14) by U_∞ and find w_s

$$w_s = \frac{D}{L}U_\infty = \left[\frac{\tfrac{1}{2}\rho U_\infty^2 (f)}{W} + \frac{1}{\tfrac{1}{2}\rho U_\infty^2 \pi}\left(\frac{W}{eb^2}\right)\right]U_\infty \qquad (16)$$

This form provides the final explanation for why the ideal Griffith 72 and LFC polars in Figure 19 are so near the same. Note that in (16) the U_∞^2 terms inside the brackets are indicated speeds because they are multiplied by ρ but the U_∞ factor outside the bracket is true speed. Eq. 16 is correct because best performance is at a particular indicated airspeed but if the true speed becomes higher as at high altitude the sinking speed will be greater. In summary it might be said that if one had such a thick airfoil that he could structurally double the aspect ratio but if its section drag coefficient also was double that of some base airfoil, no gain in performance would be made, assuming e and other lesser factors remained unchanged.

For comparison purposes the polars are all gathered together in the table below.

(Continued on next page)

CASE	POLAR EQUATION	L/D MAX	C_L for L/D MAX
Base AR = 36	$C_D = 0.0074 + 0.0110\,C_L^2$ (State of the Art)	55	0.82
LFC AR = 36	$C_D = 0.0039 + 0.0110\,C_L^2$	76	0.59
LFC + Solar Cells AR = 36	$C_D = 0.0020 + 0.0110\,C_L^2$	106	0.43
30% Griffith Ideal Drag, AR = 36	$C_D = 0.0046 + 0.0110\,C_L^2$	70	0.65
30% Griffith Ideal Drag, AR = 72	$C_D = 0.0083 + 0.0055\,C_L^2$	74	1.23
30% Griffith Realistic Pumping Efficiencies, AR = 72	$C_D = 0.0157 + 0.0055\,C_L^2$	54	1.69

Other systems are conceivable besides the two just discussed. In fact, if suction is used there are several other possibilities. One for instance is just to have the maximum thickness very far back and then use a conventional but abrupt convex fairing to the trailing edge. In this region if sufficiently strong distributed suction is applied it should be possible to prevent both separation and transition. Here suction would be needed on only the final 20% or so. In fact van Ingen et al[11] are investigating such extreme airfoils for use on the smaller commercial airplanes, but being much higher Reynolds numbers he assumes turbulent flow in the pressure recovery region.

Such a system is not likely to exceed the Griffith airfoil performance by much if both are turbulent behind 80% chord but of course the distributed suction requires a very special surface. If laminar flow is sought the surface will be even more special and it is hard to see how full laminar flow could be developed in the presence of ailerons and flaps.

Laminar boundary layer control has raised the L/D max from 55 to 76 using the LFC system including losses, but using available test information in a similar fashion for the Griffith airfoil did not lead to any improvement in the case studied. But the author does not think the Griffith system should be dropped. It is so new and so radical that it needs at least a man-year's theoretical study by someone who already has access to a suitable inverse airfoil computer program. Many answers can only come from tests, such as slot losses and performance, but very many questions can be answered by theoretical study. What is the best thickness? Where should the slots be? Can suitable airfoils be made with only one suction slot? Or if a slot is desired on the bottom side to provide a flap gap, can the suction and pressure jump be much weaker? What are the effects of camber and angle of attack? It is not hard to imagine pumping drag being reduced 50% for some kind of optimum airfoil design. For instance if by proper design the 30% airfoil could be made with just one slot sucking only half as much but at the same suction pressure the new polar equation for the Griffith 72 wing would be

$$C_D = 0.0101 + 0.0055\, C_L^2$$

giving an L/D max of 67 at $C_L = 1.35$. Of course the pumping drag might go up due to off-design requirements. The British looked at some of these questions but only in an exploratory way.

So far I have been talking only about the Griffith airfoil with its conventional flow, turbulent behind the slots. But at glider Reynolds numbers it may be possible to get 100% laminar flow by means of careful airfoil design. For the 16.2% airfoil of Fig. 11 the wind tunnel tests showed that full laminar flow occurred up to a Reynolds number of about 1.4×10^6. Then as Reynolds number increased the transition point gradually moved forward to 80% chord at $R_c = 4 \times 10^6$, meaning there was still 10% laminar flow, the slot being at 70% chord. On the 30% thick airfoil 100% laminar flow occurred to $R_c = 0.96 \times 10^6$, reducing to 93% at $R_c = 2.88 \times 10^6$. Thus it is apparent that at glider Reynolds numbers there is a good possibility that 100% or near 100% laminar flow could be obtained. If it could be, a very rough estimate of the reduced drag value indicates the L/D max for the realistic AR = 72 case will increase from 54 to 56 and the single slot case just mentioned from 67 to over 69.

The reason for the early transition is Görtler type instability caused by the concave curvature behind the slot. If it is recognized that there is some possibility of obtaining full laminar flow behind the slot some modifications can be made. The airfoils of Figure 11 and 12 both have very steep favorable gradients behind the slot. These were incorporated to get laminar flow, which possibility was destroyed by the Görtler instability. Hence the favorable gradient is not needed. If the velocity were constant behind the slot at a selected upper lift coefficient, since the laminar run in terms of Reynolds number for this part is only 200,000 or 300,000, full laminar flow behind the slot should exist except for the Görtler trouble. If the rear velocities were indeed held constant the pressure jump should be less, see Figures 11 and 12, meaning the concave curvature would be decreased and therefore the degree of Görtler instability. Moreover if the pressure jump is decreased, the amount of suction and pumping power will be reduced slightly. Hence the outlook is fairly optimistic for obtaining full laminar flow on Griffith airfoils at glider Reynolds numbers. Much study would be needed but all of it is within our present analytical capabilities. Again we mention that it may be possible to eliminate suction on the lower side of the airfoil. In fact the GLAS II[9] has only one slot.

Of course if the theoretical studies justify, then several wind tunnel tests of airfoil designs are needed. Details of the slot design, loss assessment, transition and the like should be investigated as well as obtaining conventional force data. The LFC case is relatively solid, the L/D max of 76 prediction coming from test data and reasonable efficiencies. The big problem is building a suitable wing. In short we can build the Griffith wing but do not know much about the aerodynamics. We know the aerodynamics of the LFC system but do not know how to build it. As further support for this statement we note that in 1949 Pfenninger ran completely successful tests on a 17% thick airfoil and the drag was almost as low as for the 10.5% thick section of Figure 4.[12]

An item that has only casually been studied is the pumping drag as given by (2)

$$C_{\Delta \text{pumping}} = \frac{C_Q(1-C_{P_2})}{\eta(1-a)} \tag{2}$$

Obviously according to this equation reducing the necessary C_Q is of major importance, but a more lightly loaded windmill as exemplified by a lower value of —a— will make reductions in pumping drag. So the windmill and pump should receive just as much attention as the airfoil.

I hope I have brought out some of the possibilities and problems for this kind of effort. Because many of the problems are practical, that is, constructional, development of such a sailplane falls into the hands of the designer to a considerable extent, at least after further airfoil wind tunnel tests. Needless to say, the same relative gains could be made on Standard Class or lesser sailplanes. While improvements still can probably be made by using different but more or less conventional airfoils, nothing is comparable to airfoils using some kind of laminar flow control.

Moreover it will be noticed that all the predictions have been made for a conventional configuration. There are unconventional ones such as a canard or tailless and these have not been considered. The tailless is intriguing because a higher percent of the total wetted area could be laminarized. But the wing has to be compromised to obtain trim, stability and good flying qualities. Proper design and analysis of this type was beyond the scope of this paper, but a tailless version with laminar flow control might offer still further improvement.

APPENDIX

THE PUMPING DRAG OF AN LFC SYSTEM FOR A SAILPLANE

The system being analyzed is illustrated in Figure 9. A certain quantity of air Q cu. ft./sec. is flowing inside the wing at the entrance to the pump, Sta. 2. It passes through the pump and discharges at Sta. 3, which has negligible losses between it and the final discharge. The windmill runs the pump, so first consider the windmill. According to the analysis of Ref. 10 the power output and drag of an ideal windmill are

$$P_{WM} = 2\pi R^2 U_\infty{}^3 (1-a)^2 a \qquad (a1)$$

$$D_{WM} = 2\pi R^2 \rho U_\infty{}^2 (1-a) a \qquad (a2)$$

where R is the windmill radius and a is a measure of the disk loading. The power of the windmill is readily related to its drag by means of (a1) and (a2), namely

$$P_{WM} = D_{WM} U_\infty (1-a)(\text{Ideal}) \qquad (a3)$$

The input power required by the pump is, where H represents total head, η_p is pump efficiency and η_{WM} is windmill efficiency

$$P_p = \frac{Q(H_3-H_2)}{\eta_p} = \eta_{WM} P_{WM} \qquad (a4)$$

Also the velocity head at the entrance to the pump will be or should be low so $H_2 = p_2$, and by definition $H_3 = p_\infty + \frac{1}{2}\rho V^2$. Then (a4) can be solved for V_3, yielding

$$V_3 = \sqrt{\frac{2}{\rho}} \left[p_2 - p_\infty + \frac{\eta_p \eta_{WM} P_{WM}}{Q} \right]^{1/2} \qquad (a5)$$

The drag of this discharge air is

$$D = \rho Q (U_\infty - V_3) \qquad (a6)$$

$$= \rho Q U_\infty - \rho Q \sqrt{\frac{2}{\rho}} \left[p_2 - p_\infty + \frac{\eta_p \eta_{WM} P_{WM}}{Q} \right]^{1/2}$$

For the total drag of the pumping system we must add in the drag of the windmill, which is provided by (a3) yielding

$$D_{\substack{TOTAL \\ PUMPING}} = \rho Q U_\infty - \rho Q \sqrt{\frac{2}{\rho}} \left[p_2 - p_\infty + \frac{\eta_p \eta_{WM} P_{WM}}{Q} \right]^{1/2} + \frac{P_{WM}}{U_\infty (1-a)} \qquad (a7)$$

This quantity can be differentiated again P_{WM} to find the condition for minimum drag as well as its value. We obtain, where we now lump η_p and η_{WM} into a single term η

(Continued on next page)

$$D_{\substack{TOTAL\ MIN.\\ PUMPING}} = \rho Q U_\infty \left[1-\eta(1-a)\right] + \left[\frac{\frac{\rho\eta^2(1-a)^2U_\infty^2}{2} - (p_2-p_\infty)}{U_\infty \eta(1-a)}\right] Q \quad (a8)$$

Eq. (a8 is a final general expression but it is better to convert it to coefficient form. Let

$$D_T = C_{D_{PUMPING}} \frac{\rho}{2} U_\infty^2 S, Q = C_Q S U_\infty, C_{p_2} = \frac{p_2 - p_\infty}{\frac{1}{2}\rho U_\infty^2} \quad (a9)$$

Then (a8) reduces to

$$C_{D_{PUMPING,\ MIN.}} = C_Q \left[2-\eta(1-a) - \frac{C_{p_2}}{\eta(1-a)}\right] \quad (a10)$$

This is the fully general formula for toal minimum pumping drag in coefficient form.

It is of course of interest to put the more general case (a7) into coefficient form. It will be found that a reference condition for windmill power is when just enough power is supplied so that there is no jet reaction, that is, when $V_3 = U_\infty$. Then by (a5) this power is, putting it in coefficient form

$$P_{WM} = \frac{\rho U_\infty^3 S\, C_Q(1-C_{p_2})}{2\eta} \quad (a11)$$

$$\text{when} \quad V_3 = U_\infty$$

This condition is so simple it becomes a basic reference case. Then for use in (a7) let the power be referred to (a11), that is let

$$P_{WM} = \frac{K\rho\, U_\infty^3 S\, C_Q(1-C_{p_2})}{2\eta} \quad (a12)$$

Then (a7) reduces to

$$C_{D_{\substack{PUMPING\\ GENERAL}}} = C_Q \left[2-2\left[C_{p_2} + K(1-C_{p_2})\right]^{1/2} + \frac{K(1-C_{p_2})}{\eta(1-a)}\right] \quad (a13)$$

For the special case where the windmill power is just enough to make $V_3 = U_\infty$ the only drag is the windmill drag. Then from (a11) and (a3) we find

$$C_{D_{PUMPING}} = \frac{C_Q(1-C_{p_2})}{\eta(1-a)} \quad (a14)$$

$$V_3 = U_\infty$$

This is exactly the result supplied by (a13) for $K = 1$. Because it does not indicate excessive pumping drag, eq. (a14) is very convenient for studies. In one case studied the minimum was found to be about 85% of that indicated by (a14). Eq. (a10) ratioed to (a14) indicates such values. Also it is very interesting to note that eq. (a14) indicates about the same values of pumping drag of those conventional for a propeller airplane, ignoring the η factor. There under normal assumptions the value would be $C_Q(1-C_p)$. In airplane analysis any pump efficiency effects are cancelled out by propeller efficiencies. Here we must retain them and with typical values of the denominator the pumping drag is nearly double that for a propeller airplane.

ACKNOWLEDGEMENTS

Being retired, this writer has no ready access to typing and illustration help. Therefore he is especially grateful to Shari von Delden for her typing help and Paul Pearce and his group for his help on the figures. All are members of McDonnell Douglas Astronautics Corporation which approved the help. Also it should be noted that this paper grew out of lunchtime discussions with Danny Pavish, currently Program Chairman for the Los Angeles Section of the Southern California Soaring Association.

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