A METHOD FOR PREDICTING INDOOR MODEL DURATION

DOUG MC LEAN

SUMMARY

This paper describes a method for estimating performance of rubber powered indoor duration models. The method is based on a combination of empirical flight data and simple formulas for prop efficiency, aerodynamic drag, and rubber motor energy storage. The method is easy to use, and although parts of the analysis are admittedly crude, it can provide reasonably accurate estimates of the effects on duration of many important design variables, including wing and tail aspect ratios, moment arms, c.g. location, rubber motor weight, prop diameter and RPM, and the amount of bracing wire.

A great deal of trial-and-error effort and development time have been saved by using the method to compare design features by calculation rather than by flight test. The performance potential of biplanes in the Penny-Plane event was first indicated by calculations and has since been demonstrated in flight. On the question of optimum wing chord for monoplanes in the Penny-Plane and one gram FAI events, calculations for a wide range of wing chords indicated only small differences in potential performance.

INTRODUCTION

The development of the calculation method described here began in 1971, not long after the Penny-Plane event and the one gram weight rule for FAI Microfilm models were introduced. The rules for both of these events specify a minimum airframe weight and a maximum wing span, requirements which had no precedent in the history of indoor model design. It appeared from the beginning that these new requirements would lead to different)possibly radically different) design compromises from those that had evolved over many years in the absence of weight rules, and that optimum configurations for the new events could be quite different from conventional non-weight-rule models.

Before the Penny-Plane and one gram FAI events came along, the conventional configuration that had evolved in the non-weight-rule events had become pretty well optimized. A competitive model in any of these events (AMA stick, paper stick, 90 cm FAI, Easy B, etc.) was invariably a conventional tractor monoplane with a pylon mounted wing, a short motor stick compared to the wing span, a tail boom of about the same length as the motor stick, and a horizontal tail of between 30% and 40% of the wing area. Successful designs could differ from each other in some details, but to be competitive, a design had to fall within certain limits. In the absence of weight restrictions, this conventional configuration represents an optimum compromise between aerodynamic performance and structural weight. In the early 1960's Max Hacklinger carried out an extensive engineering analysis (Reference 1) which essentially supports the above statement. His analysis did not lead to any dramatic design breakthroughs; he succeeded mainly in showing that conventional designs in existence were already close to optimum. Conventional non-weight-rule models had thus reached a highly developed state in which the only improvements left to be made were restricted to relatively subtle refinements in structures, prop design, flying technique, and so on, items which are difficult to analyze theoretically and which can really only be improved by cut and try methods.

By contrast, the situation in the Penny-Plane and one gram FAI events, when they were introduced, was wide open. All that was certain was that the optimum configurations had not yet been found, and they probably still haven't. Rather than wait for years of evolution to produce the optimum designs by trial and error, there was a strong incentive to develop a method that could be used to compare the merits of different designs without having to build and fly all of them. The method presented here is an attempt in that direction. It has provided some interesting insights into indoor duration in general, and has been an important factor in the development of a successful series of tandem and biplane Penny-Plane designs.

THE CALCULATION METHOD

The basic idea behind the method is that duration is proportional to the energy initially stored in the rubber divided by the thrust power (thrust x speed) required to stay aloft in level flight, or

$$t = \eta_{av} E/P \tag{1}$$

If you assume that the descent portion of the flight makes up for the extra energy spent in the climb and that the model flies the entire flight at its optimum airspeed (an ideal that we try for in practice), then the proportionality constant η_{av} can be interpreted approximately as the prop efficiency (thrust work output/shaft work input) averaged over the whole flight. The validity of the method doesn't depend on the correctness of this interpretation of η_{av}, however, and it is more nearly correct to view η_{av} simply as a quantity defined by Equation (1).

To take into account the effects of prop design (diameter, RPM, torque, etc.) on the factor η_{av}, it is helpful to break it down into two factors:

$$\eta_{av} = F\eta_p \tag{2}$$

Here η_p is the optimum prop efficiency that could be achieved in cruise, which I calculate from the thrust, airpseed, diameter, and RPM, using optimum prop formulas (see von Mises' aerodynamics text, Reference 2), which are given in Appendix I. The von Mises formulas give a pretty reliable indication of the effects of prop diameter and RPM on prop efficiency when a given thrust is required. I tested the formulas against some prop efficiency measurements made on an indoor model in cruising flight by Hacklinger (Reference 1), and the agreement was very good. The factor F is a fudge factor to take into account the fact that the prop doesn't operate at optimum cruise efficiency all the time. If we had a constant torque power source and could fly our models at a constant cruise, the factor F would take on the value 1.0 for an optimum prop. In actual practice, mostly because of the effects of climbing and descending, values of F between .5 and .85 can be achieved, depending on the ceiling height.

For the energy E stored initially in the rubber, I use the maximum energy storage possible at full turns as a funtion of the motor weight only, based on torque curves measured by Bob Platt (published in "Indoor News and Views," sometime in 1971), I assume that Bob had some pretty good quality Pirelli, and I regard my calculations as an indication of performance potential, given good quality rubber. The energy stored is assumed to be proportional to the motor weight:

$$E = K_m W_m \tag{3}$$

where

$$K_m = 30{,}000 \text{ inches}$$

for any consistent combination of units for E and Wm.

The thrust power P required to cruise in level flight is where the airplane aerodynamic performance estimates come in. In steady, level flight, the total lift must equal the all-up weight W, and the thrust required is equal to the total drag. This results in the following expression for the power required:

$$P = \left(\frac{2}{\rho}\right)^{1/2}\left(\frac{W}{S}\right)^{1/2} \frac{C_D}{C_L^{3/2}}\, W \tag{4}$$

By combining Equations (3) and (4) we get the energy stored/power required ratio appearing in Equation 1:

$$\frac{E}{P} = K_m \left(\frac{\rho}{2}\right)^{1/2}\left(\frac{S}{W}\right)^{1/2} \frac{C_L^{3/2}}{C_D}\, \frac{W_m}{W} \tag{5}$$

The lift coefficient CL and the drag coefficient CD appearing in Equations (4) and (5) are estimated by a combination of empirical data and aerodynamic theory. What we really want to calculate, of course, is the power required to cruise at the optimum CL, or the CL at which the power required is a minimum for the particular airplane in question. However, I don't try to determine the optimum CL for each design, since it is too sensitive to the dependence of profile drag on CL, which in turn depends on the airfoil. Instead, I simply assume that the main lifting surface operates at CLF = 1.0, which seems reasonable for indoor model wings, based on Hacklinger's glide measurements (Reference 1). By balancing the pitching moments about the c.g., I calculate the CLR of the tail and then a total CL for the whole airplane (keeping proper track of the appropriate reference areas). I use a formula based on Hacklinger's measurements for profile drag as a function of Reynolds number and simple lifting line formulas for the induced drag of both the wing and the tail, taking into account the effect of wing downwash on the induced drag of the tail. For the induced drag of biplanes and tandems I use biplane formulas given in von Mises' text (Reference 2). To complete the drag calculations I assume that wing posts and bracing wire are simple circular cylinders and estimate their drag based on their Reynolds numbers. The formulas used in the lift and drag calculations are summarized in Appendix II.

Returning now to the basic duration formula, Equation 1, we see that I've borrowed or invented cookbook formulas for everything but the fudge factor F. In order to use Equation 1 to make a direct prediction of the duration potential of a proposed model design flying under a given ceiling height, we need a realistic way of determining the appropriate value of F. To do this, I've used flight data for a number of existing designs to determine an empirical curve-fit for F. Existing designs were chosen for which a long enough history of flight experience was available to indicate that the flight times reported represented something close to the models' maximum potential. The design variables of each airplane were plugged into the formulas for CL and CD, and these values, along with the known flight times t, turned Equation (1) into an equation for F. The values of F thus determined from flight data were correlated with ceiling height, as I'll explain below, determining a curve-fit for F which can now be used to make duration estimates. This use of actual flight data to "calibrate" the method tends to counteract the crudeness and uncertainty in some of the formulas and assumptions.

The quantity which was found to correlate well with the factor F is a kind of dimensionless ceiling height:

$$\mathcal{H} = \frac{h}{483\,\frac{W_m}{W}} \tag{6}$$

The constant 483 in the denominator is not crucial; any **constant would do. 483Wm/W happens to be the height in ft.** which the airplane could climb if all of the rubber motor energy stored in the torque peak above the average torque on the torque curve were converted into altitude at 100% efficiency. (See Figure 1). Since cruise usually occurs somewhere near the average torque, the energy represented by the shaded area under the peak is a measure of the shaft work available for climbing and is thus a meaningful quantity with which to compare the ceiling height. 483 Wm/W is the height to which this amount work could lift the all-up weight of the airplane. The factor Wm/W thus accounts for the fact that a given ceiling height looks different to models of different motor weight fractions.

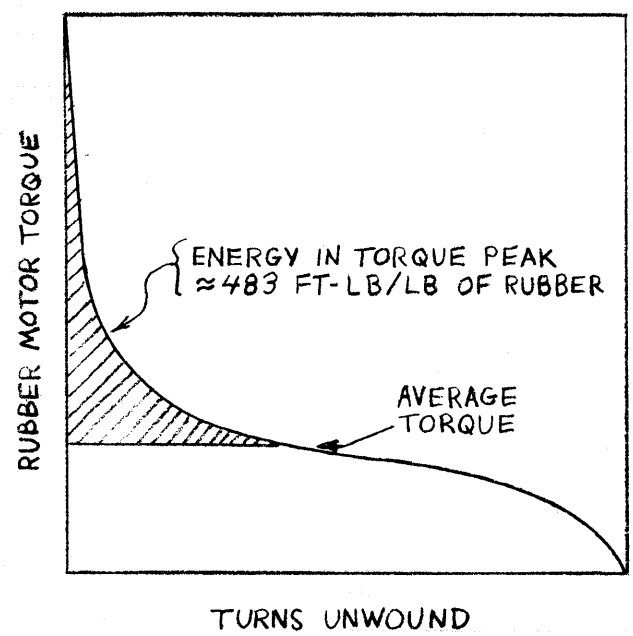

Figure 1: Rubber motor torque curve illustrating approximate shaft work available for climbing.

The proof of all this is in the actual correlation of F versus H, which is shown in Figure 2. The data points fall surprisingly close to a single curve. To obtain values of F for use in performance predictions, I drew a curve roughly through the upper limit of the data points. I interpret the curve as an indication of the maximum performance possible with the current state of the art in prop technology. As technology improves, I'll probably have to move the curve up. Note that at the low ceiling end of the scale the lower values of F cannot be interpreted strictly as a reduction in prop

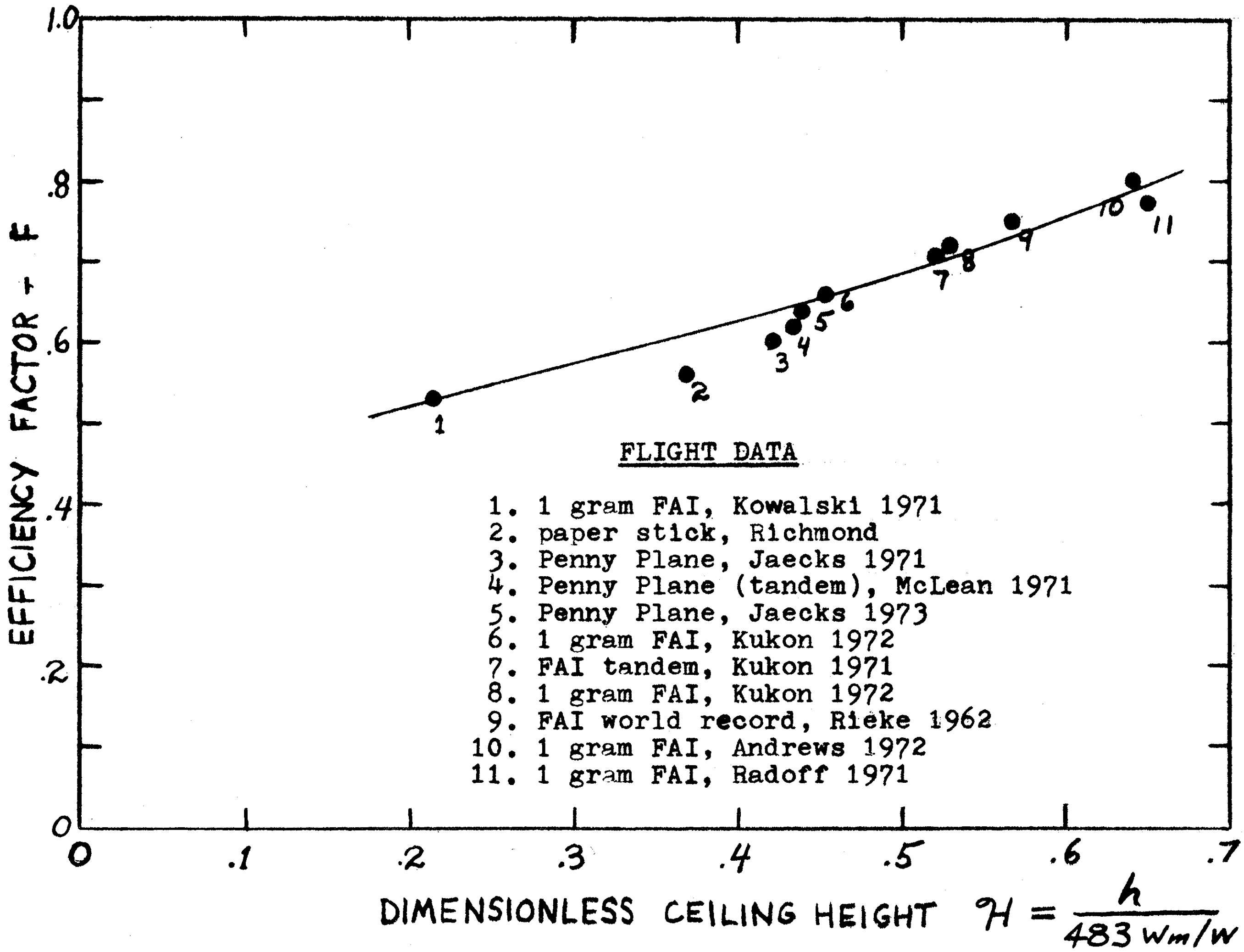

Figure 2:Correlation of efficiency factor F with ceiling height.

efficiency. Whereas the maximum energy storage E of the rubber is used in the duration formula, low ceiling flights are usually made with only partial turns and thus with an initial energy storage less than the maximum. This effect is not distinguished in the formulas and is responsible for at least part of the reduction in F observed at lower ceiling heights.

USE OF THE METHOD

Predicting the performance of a proposed new model design simply involves plugging the proposed design variables into the appropriate formulas until all of the quantities in Equation (1) have been determined. The lift and drag coefficients CL and CD are calculated from the formulas of Appendix II. E/P can then be calculated from Equation (5). The cruise efficiency η_p of the prop is found from the formulas of Appendix I, and the motor weight fraction and ceiling height combine to determine the factor F from Figure 2. Care must be taken to keep the physical units consistent, but aside from this, the method is not difficult to use. A pocket calculator makes it easier, but even with a slide rule and scratch paper it can be done fairly quickly.

For the purpose of evaluating different designs within a given model class, it usually suffices to compare values of the power P required for cruise, without carrying out the full duration prediction described above. To the degree of approximation represented by the correlation in Figure (2), the factor F depends only on the motor weight fraction Wm/W and the ceiling height. Thus in order to compare two designs with the same Wm/W at the same ceiling height (e.g. to compare two Penny-Planes or two FAI designs), the values of F are equal, and only the remaining terms of Equation (1),

i.e., η_p/P

need to be compared for the two designs. For most designs in a given model class, the values of η_p will be nearly equal, and good approximate comparisons can be made simply by comparing values of P.

SAMPLE CONCLUSIONS

Optimum Chord for Monoplanes: In the Penny-Plane and 1 gram FAI events, with their fixed airframe weight — fixed wingspan rules, the choice of wing chord involves a trade-off between wing loading and induced drag. Increasing the chord decreases the wing loading, a factor which by itself would increase duration. But increasing the chord decreases the flight speed, which increases the induced drag, since the weight and wing-span are fixed. There should thus be a particular chord which represents an optimum in this trade-off situation. I've carried out calculations for Penny-Planes with chords ranging from 4½ to 8 inches and for 1 gram FAI models with chords ranging from 6 to 9 inches. The predicted performance differences attributable to chord were too small to be resolved reliably by the prediction method. It appears that chord has relatively little influence on performance potential (provided, of course, that the prop and motor are well matched to the particular chord chosen. A combination that works well on a wide chord model might not work well on a narrower chord model, and vice versa). This conclusion is supported by the wide range of chords seen on successful models in both events.

The 1 gram FAI Tandem: A tandem wing model with 5-inch chord wings, an 18-inch motor stick, a 7-inch tail boom, and a vertical separation between the wings of at least 1/4 wingspan (see Figure 3) appeared to be a promising configuration for the 1 gram FAI EVENT. Calculations indicated that this configuration should have an advantage over the best monoplanes, but the predicted advantage is only 8%, which is not large compared to the uncertainty of the prediction method. The design would have to be developed and flown extensively to determine whether the advantage really exists or not. A few years ago John Kukon put a lot of work into developing a tandem design along the lines described above, and his latest version was published in ***American Aircraft Modeler*** **(Reference 3. I did the calculations and wrote the section on theory, but the editor omitted my name from the byline by mistake). John found the tandems to be a tough structural problem. When he built them light enough to be competitive, they tended to be too fragile to fly consistently, and in the last couple of years John has gone back to conventional monoplanes.**

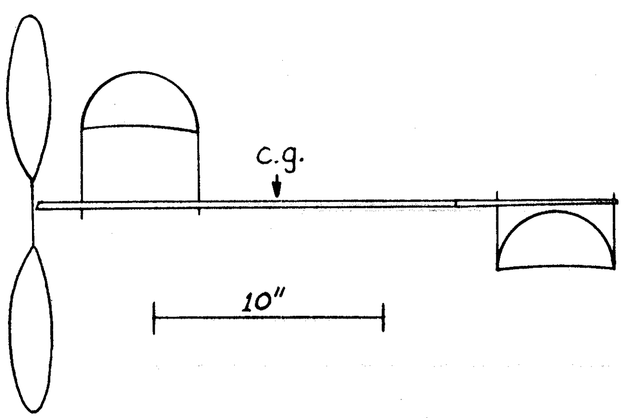

Figure 3:FAI 1 gram tandem configuration.

Tandem Penny-Planes: Back in 1971 I designed a tandem Penny-Plane with 4½-inch chord wings, 3½-inch wing posts for both wings (front wing up, rear wing down), and with the front wing mounted far forward, just barely clearing the prop (see Figure 4). The model had to be built light and ballasted in the rear in order to load the rear wing enough to take advantage of the tandem concept. The calculations indicated that this configuration should have about a 7% edge over the best monoplanes, which is not spectacular, but which seemed to be worth investigating. My first version of the design was the first Penny-Plane I know of to do over 12 minutes in competition, with a flight of 12:02 under an 80-foot ceiling in Philadelphia in November, 1971. John Kukon, flying the same design, did 11:56 at the same contest. The design has done over 13 minutes on several occasions at Lakehurst. More recently, John increased the front wing chord to 6 inches, keeping the same rear wing, and has done over 15 minutes at Lakehurst.

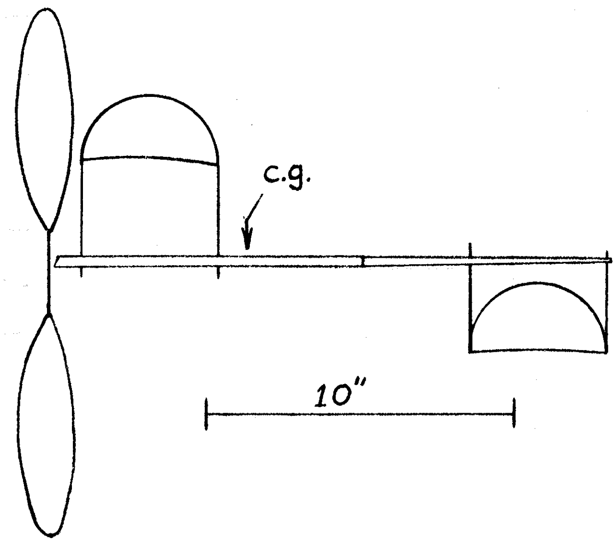

Figure 4: Penny-Plane tandem configuration.

Biplanes: A biplane configuration with a vertical gap between the main wings of 1/4 to 1/3 wingspan is a very effective way of getting around the wing loading-induced drag trade-off mentioned earlier in conjunction with monoplanes. In 1974 I developed a biplane Penny-Plane design with a 4½-inch chord, 8-inch wing posts, and a 4½-inch vertical gap between the wings (see Figure 5). In order to increase stability and allow a farther aft c.g., the stab is mounted below the stick on 3½-inch posts, as in the earlier tandem designs. A three-view of this model was published recently in "Indoor News and Views" (Reference 4). The prop thrust and wing drag, combined with the long wing posts, produce a substantial nose-up pitching moment which increases the share of the load carried by the tail, and which should have a favorable effect on duration. This effect, which is generally negligible for conventional configurations and has been ignored in the formulas given in Appendix II, is appreciable for biplanes with long wing posts and was taken into account in the calculations for the biplane Penny-Plane. The calculations indicate an advantae of 30% over the best monoplanes. The advantage demonstrated so far in flight has not been as dramatic as 30%, but it has been substantial. The best flight so far was an unofficial 16:03 during the last weekend of the Aerolympics in July, 1974 at Lakehurst. I expect that biplane Penny-Planes will eventually do better than that, since some very good fliers, including Dennis Jaecks and John Kukon, are now flying them.

In the 1 gram FAI event, the advantage of a biplane does not appear to be as clear-cut as in Penny-Plane. An FAI biplane, like a tandem, is a tough structural problem, and, as a result, biplanes tend to be overweight, a situation which can easily defeat any advantage the biplane might have. **(Clarence Mather's very promising design of a few years ago was one exception to this.)Still,the calculations indicate that a biplane with a 4-1/2 inch chord, a sufficient vertical gap (6 inches, say), and a minimum amount of strut work and**

bracing wire (for drag reasons) could have an advantage of 10% or more over a monoplane. For someone with a real talent in structures (not me!) it could be worth investigating.

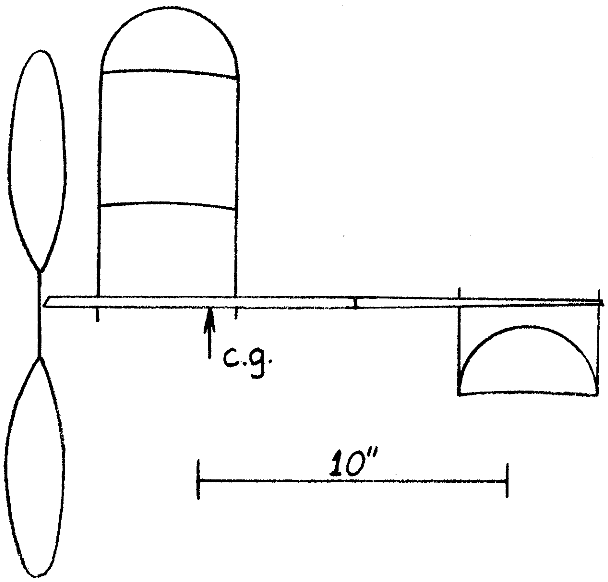

Figure 5:Penny-Plane biplane configuration.

Optimum Propeller Diameter for Cruise: The propeller formulas given in Appendix I provide a reasonably accurate way of assessing the effect of diameter and RPM on the prop efficiency in cruising flight. The thrust required to cruise is fixed by the weight and aerodynamic performance (CL and CD) of the model. The duration of the model can be estimated even before η_p is calculated because η_p generally falls within a narrow range (.74-.79). For a rubber motor of given cross-section and weight, cruise RPM can be estimated from the flight duration and the turn capacity of the motor. Given the thrust and the estimated RPM, the efficiency η_p can be calculated (Formulas of Appendix I) for props of several different diameters, and the optimum diameter, or the diameter yielding the highest η_p , is easily found.

If we repeat the above exercise for different motors of the same weight but different cross-sections, the effects of torque and RPM on the optimum prop diameter and optimum η_p can be seen. Motors of larger cross-section (higher torque and lower RPM) call for larger diameter props and produce higher values of η_p . In fact, if we continue to increase the motor cross-section, the optimum prop diameter and the value of η_p for that optimum diameter will both continue to increase. The prop analysis by itself indicates that the bigger the motor cross-section and the larger the prop diameter, the higher the efficiency can be.

The efficiency advantage of a large motor cross-section and large prop diameter is counteracted by effects not taken into account in the above analysis. In the non-weight rule events, a high torque motor and a large prop exact a weight penalty, both in the prop weight itself and in the weight of the motor stick and the rest of the structure. In this case the optimum motor cross-section and prop diameter depend on a complicated combination of structural and aerodynamic considerations. In the Penny-Plane and 1 gram FAI events, the weight rules reduce the importance of structural weight considerations. This situation favors high torque motors and large props (Note that in Penny-Plane a large motor cross-section also is required in order to achieve a satisfactory motor weight, apart from any prop efficiency considerations). A practical limit to the motor cross-sections and prop diameters that can be used advantageously in these events is imposed by the effects of high torque on the stability and aerodynamic performance of the airplane itself.

AMA Stick, Paper Stick, Easy B, etc.: None of the aerodynamic performance conclusions in the previous sections applies to these non-weight-rule events. As I argued in the Introduction, conventional designs are already well optimized for these events. I've tried calculations onbiplanes, tandem pushers, and so on, and nothing I've tried can beat the conventional configurations. The best you can do in these events is to pick a conventional design that pleases you, refine it until you can build it lighter and better than anybody else's, and then spend the rest of your time working on props and flying technique.

APPENDIX I

von Mises' Prop Efficiency Formulas

The assumptions behind the modified momentum theory of propeller efficiency are explained in Reference 2, sections XII 3 and XII 4. The result of the theory is an expression for propeller efficiency:

$$\eta_p = \eta_i \frac{1-(4J\epsilon)/(3\pi\eta_i)}{1+(2\pi\eta_i\epsilon)/(3J)} \qquad (I.1)$$

where

$$\eta_i = \frac{2-(J^2\tau)/\pi^2}{\sqrt{\tau+1}+1} \qquad (I.2)$$

and ϵ is the profile drag-to-lift ratio of the propeller blade airfoil. For a propeller operating at a given thrust loading τ and advance ratio J, the induced efficiency given by η_i ; can be interpreted as the upper limit to the efficiency that the propeller could achieve, even if friction effects were absent, and no matter how the propeller were shaped. This upper limit to the efficiency of a "perfect" propeller, imposed by kinetic energy considerations, is analogous to the lower limit to the drag of a "perfect" wing, that is the induced drag of a wing with an elliptic load distribution. The remaining terms in Equation (I.1) account approximately for the reduction in efficiency resulting from the fact that the propeller blades experience profile drag losses in addition to the induced losses accounted for by η_i. The combined efficiency η_p is thus an approximate upper limit to the efficiency of a propeller operating at a given τ and J using a blade airfoil with a profile drag-to-lift ratio ϵ .

In the overall calculation scheme, Equations (I.1) and (I.2) are used to calculate the value of η_p needed in Equation (1) for the duration t. Once the lift coefficient CL and drag coefficient CD of the airplane have been calculated by the formulas of Appendix II, the thrust T required for cruise is known, and the flight speed V can be calculated from CL and the wing loading. For purposes of the calculation, a rough estimate of the prop rotation speed is adequate. I simply make a preliminary estimate of the duration t and assume n = N/t, where N is the maximum number of turns for the rubber motor. (The motor size is one of the design variables which must be known). For ϵ I use the value 0.1, which is a reasonable assumption for the range of Reynolds numbers encountered by indoor props and which gave good agreement between the prop efficiency measured in flight by Hacklinger (Refernce 1) and the corresponding calculated value of η_p. All of the quantities required in Equations (I.1) and (I.2) are now known, and η_p can be calculated. For virtually all indoor props the value of η_p will fall between .74 and .79, with values around .77 being the most common. If you're making calculations for a Penny-Plane with a prop diameter over 16 inches or an FAI model with a prop over 18 inches, it's a pretty good assumption to skip the η_p calculations and simply use a value of .77.

Formulas for CL and CD

The formulas I use for calculating the overall lift coefficient CL and overall drag coefficient CD of an indoor model are given in this Appendix in the order in which they would be evaluated in an actual calculation. The symbols used are defined in the symbols list, and the geometric variables are illustrated in Figure (II.1). Note that the c.g. location coordinate X is negative when the c.g. is behind the wing's aerodynamic center. Methods for locating the aerodynamic center of a lifting surface (wing or tail) can be found in any standard aerodynamics text, so I won't go into that here. The mean aerodynamic chords CF and CR of the wing and tail are adequately approximated by the average chord given by area/span for each surface.

All of the formulas have been derived under the assumption of a lift coefficient of 1.0 for the forward lifting surface (wing):

$$CLF = 1.0$$

By balancing the pitching moments about the c.g. and assuming the wing and tail airfoils have pitching moment coefficients of -.10 and -.05 respectively (we generally use less camber on tails than on wings), we find the lift coefficient of the tail to be:

$$C_{LR} = \frac{-(x/C_F) - 0.10 - 0.05(S_R C_R / S_F C_F)}{\{1 + (x/\ell)\}(S_R \ell / S_F C_F)}$$

The overall lift coefficient is then

$$C_L = \frac{1 + (S_R C_{LR}/S_F)}{1 + (S_R/S_F)}$$

which leads to a flight speed in feet/sec.:

$$V = 16.7 (W/C_L S)^{1/2}$$

for W in grams and S in square inches.

For monoplanes, the induced drag of the wing and tail can be calculated separately and then combined for the total CDI as follows:

$$C_{DIF} = 1/\pi A_F$$

$$C_{DIR} = (C_{LR}^2/\pi A_R) + 2 C_{LR} C_{DIF}$$

$$C_{DI} = \frac{C_{DIF} + (S_R C_{DIR}/S_F)}{1 + (S_R/S_F)}$$

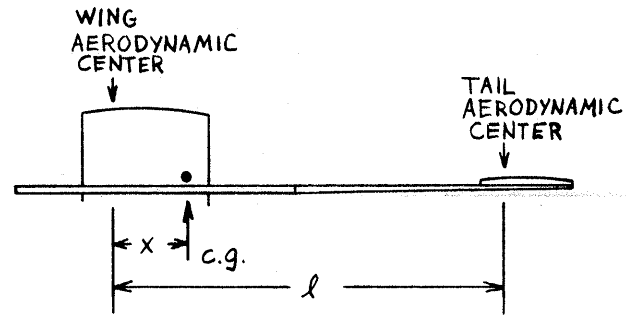

Figure (II.1): Definitions used in CL calculation

For a biplane with wings of equal area and span, each with aspect ratio AF, and a vertical gap G, the calculation is the same as for a monoplane except that

$$C_{DIF} = \begin{cases} 1.43/\pi A_F & \text{for } G = b/4 \\ 1.35/\pi A_F & \text{for } G = b/3 \end{cases}$$

For a tandem with wings of equal area and a vertical gap G, the total induced drag is calculated in one step:

$$C_{DI} = \begin{cases} (1.0 + C_{LR}^2 + .86 C_{LR})/2\pi A_F & \text{for } G = b/4 \\ (1.0 + C_{LR}^2 + .70 C_{LR})/2\pi A_F & \text{for } G = b/3 \end{cases}$$

The following formulas account for the effects of Reynolds number and lift coefficient on profile drag. The dependence on the square root of Reynolds number comes from the assumption that the wing and tail boundary layers are mostly laminar, and the constants were derived to be consistent with the glide measurements made by Hacklinger (Reference 1).

$$C_{DPF} = 6.2 R_F^{-1/2}$$

$$C_{DPR} = (4.3 + 1.9 C_{LR}) R_R^{-1/2}$$

where $R_F = C_F V/\nu$, $R_R = C_R V/\nu$

and $\nu = 15.88 \times 10^{-5}\ \text{ft}^2/\text{sec}$

The total profile drag coefficient is then

$$C_{DP} = \frac{C_{DPF} + (S_R C_{DPR}/S_F)}{1 + (S_R/S_F)}$$

The drag coefficient of bracing wire is derived from data for circular cylinders at low Reynolds numbers (Reference 5). In the Reynolds number range encountered by most bracing wire (.3 and 1.6) the drag coefficient can be approximated by

$$C_{DW} = 13 - 28 \log_{10} R_W$$

where $R_W = d_W V/\nu$

Wing posts have a Reynolds number of around 100, at which the drag coefficient of a cylinder is about 1.2. The combined drag coefficient for bracing wire and wing posts is then

$$\Delta C_D = (l_w d_w C_{DW}/S) + 1.2 S_{WP}/S$$

In the overall drag coefficient of the airplane, I neglect the drag of the fuselage and vertical tail, which should be insignificant compared to the other contributions. The final formula is thus:

$$C_D = C_{DI} + C_{DP} + \Delta C_D$$

which completes the calculation of CL and CD.

SYMBOLS

AF aspect ratio of forward wing = b^2/SF or $2b^2/SF$ for a biplane.
AR aspect ratio of rear lifting surface or tail = bR^2/SR
b wingspan
bR tailspan
CD overall drag coefficient
CDI overall induced drag coefficient
CDIF induced drag coefficient of forward wing(s)
CDIR induced drag coefficient of tail
CDP total profile drag coefficient
CDPF profile drag of forward wing(s)
CDPR profile drag of tail
CDW drag coefficient of bracing wire
ΔCD combined drag of wing posts and bracing wire
CF mean aerodynamic chord of forward wing
CL overall lift coefficient
CLF lift coefficient of forward wing
CLR lift coefficient of tail
CR mean aerodynamic chord of horizontal tail
dw diameter of bracing wire
E initial energy stored in rubber motor
F emperical efficiency factor (Figure 2)
G vertical gap between wings of a biplane or tandem
h ceiling height in feet
H dimensionless ceiling height (Figure 2)
J propeller advance ratio = V/nd
Km rubber motor constant (Equation 3)
l tail moment arm (Figure II.1)
lw total length of bracing wire
n propeller rotation speed (revolutions/sec)
N maximum turns for rubber motor
P power required in level flight = thrust x speed
RF Reynolds number of forward wing(s) = $C_F V/\nu$
RR Reynolds number of tail = $C_R V/\nu$
Rw Reynolds number of bracing wire = $d_w V/\nu$
S total horizontal projected area of airplane
SF total horizontal projected area of forward wing(s)
SP propeller disc area
SR projected area of tail
Swp area of wing posts projected in flight direction
t flight time (duration)
T propeller thrust force
V flight speed

W total airplane weight (including rubber)
Wm weight of rubber motor
X c.g. location with respect to wing aerodynamic center (Figure II.1)
ϵ propeller blade profile drag-to-lift ratio
η_{av} flight efficiency factor defined by Equation (1)
η_i propeller induced efficiency (Appendix I)
η_p optimum propeller efficiency in cruise = shaft work input/thrust work output (Appendix I)

ν kinematic viscosity of air = $15.88 \times 10^{-5} ft^2/sec$
ρ density of air = $33.6 \; gm/ft^3$
τ propeller thrust loading coefficient = $2T/\rho V^2 S_p$

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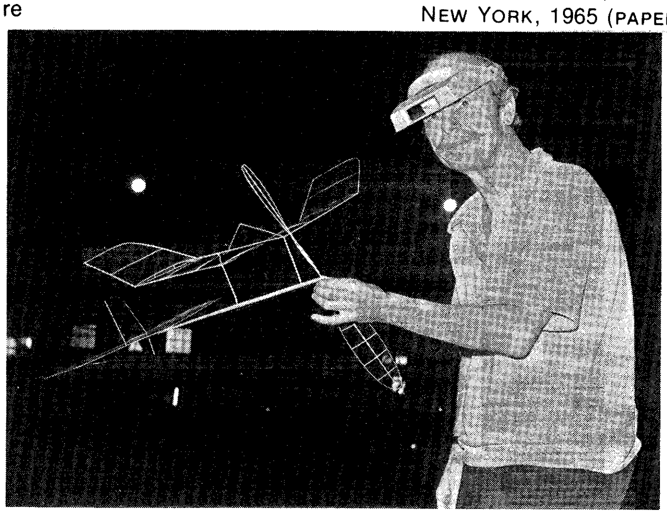

Bill Tyler at Lakehurst