OPTIMUM DESIGN OF BRUSHLESS TUBULAR LINEAR MACHINES

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Abstract – **A design method for linear tubular brushless d.c. machines is described which uses finite element techniques. The effects of variation in slot depth and pole pitch are illustrated. The finite element analysis is supported by analytical results using a simple method.**

INTRODUCTION

Conventional rotary d.c. brushless machines are finding many applications. Low voltage smart power integrated circuits have been produced for them. These chips include decoder, transistor inverter and current limiter stages in a very economical package. Linear d.c. brushless machines can also be designed [1] and an attractive long stroke linear drive system results if the smart chips are again used [2].

A previous paper [2] described such a system and modelled its dynamic performance.

It is the objective of this paper to consider the electromagnetic design of linear brushless machines using finite element modelling.

MACHINE CONSTRUCTION

A drawing of two sectional views of the machine under consideration is shown at Fig. 1. The stator is constructed from four laminated blocks and carries a two pole three phase winding formed from 6 coils. The excitation is provided by sets of permanent magnets which are stepped skewed to eliminate cogging.

MACHINE DIMENSIONS

Before the finite element study can be started the machine dimensions must be decided. In this paper they are found geometrically by considering the excitation flux levels in the various sections of the machine.

Two rotor positions are sketched on the sectional view of Fig. 2. At Fig. 2a, half the pole flux passes through the armature back iron and the excitation core. This is the case corresponding to an infinitely long core and is the minimum flux which could occur in the short stator machine geometry. At the position shown in 2b, on the other hand, the whole of the pole flux traverses the armature and excitation back iron regions and this is the maximum value that this flux could have. In practice the flux will be somewhere in between the two limiting values given above due to leakage from the ends of the machine and armature flux. In order to calculate the machine dimensions, the

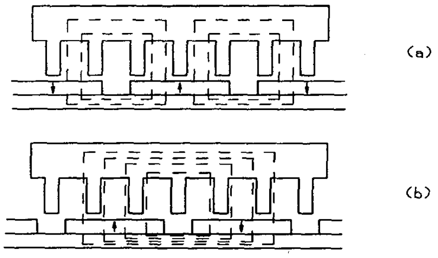

Fig. 2 Two rotor positions showing flux in backing iron

upper limit will be used. The Appendix outlines this process, showing how the assumptions of various material constants and the machine pole-pitch can be used to calculate the rest of the dimensions. Given these values, a particular slot depth may be assumed and the slot conductors designed by normal techniques [3]. A current density can then be chosen (5A/mm^2 in the cases below) and the force calculated using either a finite element package [4] or the approximate analytical method given in the Appendix.

FINITE ELEMENT MODEL

A three dimensional finite element representation is desirable, chiefly because of the stepped skewed excitation. However, a two dimensional representation can be employed with a consequential saving of computer time if the analysis of the machine is first performed [5] on an axisymmetric basis for a reference machine which has no skew. Space shifted versions of the results can then be added together to find the response of a skewed machine.

VARIATION OF FORCE WITH POSITION

The machine is fed from an inverter which is part of the integrated circuit driver. This supplies currents to the armature phases which are constant for 60 electrical degrees of travel of the excitation. Fig. 3 illustrates the effect by showing the

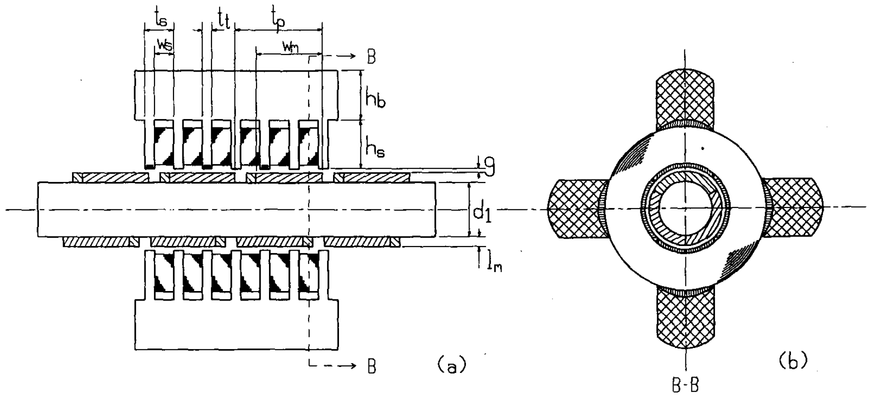

Fig. 1 Two sectional views

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armature current and pole positions at the beginning and end of an inverter switching period. The period chosen is interval five on the current waveforms of Fig. 4 when the 'a' phase current is negative, the 'c' phase current is positive and the 'b' phase current is zero. The force varies over this interval, as illustrated by the curve of Fig. 5, which was calculated by the finite element method described above. The force is the sum of the longitudinal end effect force and the force due to the armature currents. The cogging forces due to the teeth have been skewed out. It will be observed that the force peaks in the middle of the interval and it was decided to make all further calculations at this point during the design process.

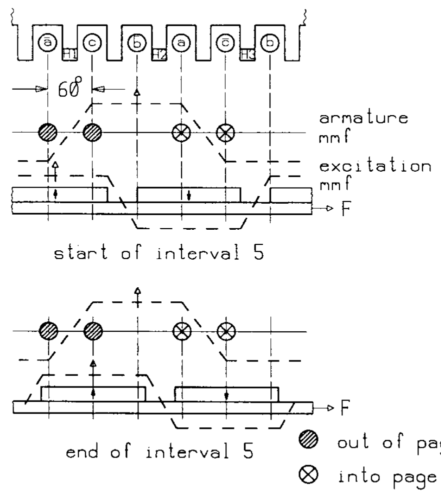

Fig. 3 Sketch of the excitation and current positions over 60° electrical

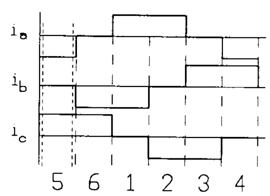

Fig. 4 Current time waveforms

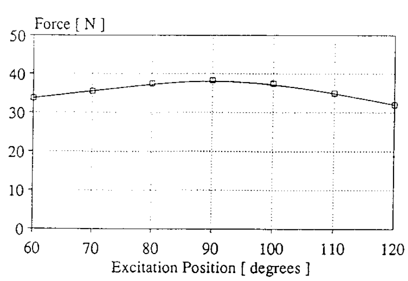

Fig. 5 Force variation over 60° in time

OPTIMISATION PROCESS

So far the force from a machine with one particular set of dimensions has been calculated. In order to find the best machine it is now necessary to calculate the variations in force when the dimensions are changed.

One technique is to choose a particular pole pitch and work out the dimensions as given in the Appendix. These define the machine completely, except for the slot depth, and it is convenient, therefore, to calculate the force at a range of slot depths looking for the best machine. The machines are employed mainly as actuators and so the standstill performance is used for this purpose.

There are many possible quality factors, but in this paper the force per volume is taken as a measure and plotted against slot depth for 3 pole pitches in Fig. 6. The volume used is the cylinder which encloses the machine.

The design process indicates that the maximum force per volume can be found at any pole pitch. The system of calculation at a fixed space position is convenient and once a design is chosen the calculations as performed for Fig. 2 can be used to find the small variations with position.

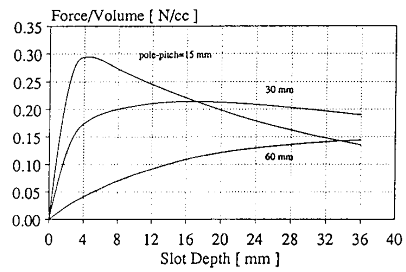

Fig. 6 FE calculation showing total force

ANALYTICAL COMPARISONS

The force per volume for the conditions of Fig. 6 were also calculated using the approximate analytical calculations given in the Appendix. The results are shown at Fig. 7 in dashed line. This analytical calculation can be compared with the finite element analysis by subtracting from the finite element calculation of Fig. 6 the end effect force. Fig. 7 shows the results of this in solid line and it will be observed that the two sets of graphs on the figure compare favourably. The end effect calculation was performed using a finite element model in which the armature currents were zero. It was checked that the force varied only slightly (2 to 3%) with the outer machine diameter which varies as the slot depth is varied.

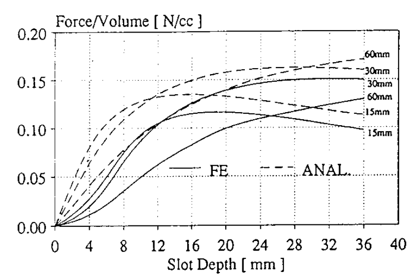

Fig. 7 FE and Analytical calculations showing Electromagnetic Force

CONCLUSIONS

A design technique has been described. This makes use initially of approximate calculations to decide the machine dimensions. Finite element analysis is then employed to calculate a series of designs to find the optimum. The work is supported by a technique based on a simple method of calculating the electromagnetic force.

APPENDIX: CALCULATION OF DIMENSIONS

Table of Symbols

B_c	excitation core density
d_1	core diameter
t_p	pole-pitch
β	ratio of the magnet width to the pole-pitch
ℓ_m	magnet length
g	airgap length
B_g	airgap flux density
g_{eff}	effective airgap length
H_m, B_m	working point magnetising force and flux density of the magnet
t_t	tooth width
t_s	slot pitch
α	ratio of the tooth width to the slot pitch
Ψ_t, B_t	tooth flux and flux density
N_s	number of slots per pole
A_{arm}	total cross-sectional back iron area
h_b	back iron height of the armature block
h_s	slot height
w_s	slot width

The diameter of the excitation core can be related to the chosen pole pitch by equating the airgap flux to the excitation core flux:

$$B_c \frac{\pi}{4} d_1{}^2 = \beta t_p \; (d_1 + 2\ell_m + 2g) \; \pi \; B_g \tag{1}$$

The magnet length can be expressed in terms of the airgap density, assuming that the relative permeability of the magnet material is unity and again neglecting leakage and fringing, that is taking $B_m = B_g$.

Whence:

$$\ell_m = \frac{g_{eff} \; B_m}{H_m \; \mu_o} \tag{2}$$

The effective airgap length can be calculated using Carter's coefficient [6].

$$g_{eff} = g \left[\frac{5g + (1 - \alpha) \; t_s}{5g + (1 - \alpha) \; t_s - (1 - \alpha)^2 \; t_s} \right] \tag{3}$$

$$\text{where } \alpha = t_t/t_s \tag{4}$$

The quantity of flux, Ψ_t, entering a tooth is equal to the pole flux (rhs of Eqn 1) divided by the number of teeth in a pole, hence:

$$\Psi_t = \beta \frac{t_p}{N_s} (d_1 + 2\ell_m + 2g) \; \pi \; B_g \tag{5}$$

$$\text{where } N_s = t_p/t_s \tag{6}$$

The radial cross sectional area of a tooth for the whole circumference can be calculated by

$$A_s = \alpha \frac{t_p}{N_s} (d_1 + 2\ell_m + 2g) \; \pi \tag{7}$$

and the tooth flux density using equations 5 and 7 as

$$B_t = \frac{\beta}{\alpha} B_g \tag{8}$$

Assuming that the whole of the pole flux passes through the armature backing iron, the total cross-sectional area of armature backing iron for four blocks must equal the radial cross-sectional area of a tooth for the whole circumference multiplied by the number of slots per pole so that:

$$A_{arm} = \alpha \; t_p \; (d_1 + 2\ell_m + 2g) \; \pi \tag{9}$$

Since the armature flux is equally shared between four blocks, the back iron height of the armature block can be calculated by:

$$h_b = \frac{A_{arm}}{(d_1 + 2\ell_m + 2g) \; \pi} = \alpha t_p \tag{10}$$

which assumes a rectangular cross-sectional area for simplicity in place of the crossed area on Fig. 1(b).

The slot width can be expressed as

$$w_s = (1 - \alpha) \; t_p/N_s \tag{11}$$

From the above equations, the dimensions of the machine can be calculated given the following material constants and assumed dimensions:

- Number of armature slots per pole (Ns) = 3
- Magnet working flux density (B_m) = 0.5 T
- Corresponding magnetising force (H_m) = 450 kA/m
- Peak airgap flux density (B_g) = 0.5 T
- Iron flux density (B_t, B_c) = 1.5 T
- Airgap length (g) = 0.5 mm
- β = 0.8

The method is to choose a pole-pitch then calculate α, t_s, w_s using equations 8, 6 and 11. Using these h_b, g_{eff}, ℓ_m and d_1 are calculated using equations 10, 3, 2 and 1 respectively.

CALCULATION OF ELECTROMAGNETIC FORCE

The net slot current, I_s, can be found from normal methods [3] assuming a particular current density, packing factor and so on.

The standstill force per armature pole is then given by:

$$F_{pp} = B_g \; [d_1 + 2\ell_m + 2g] \; \pi . 2I_s$$

which assumes that two slot currents per pole will be contributing to the force production, as sketched in Fig. 3.

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