

# A DESIGN PACKAGE FOR SINGLE-TOOTH PER STATOR POLE SWITCHED RELUCTANCE MOTORS

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**Abstract** A design package is developed for a single-tooth per stator pole switched reluctance motor, 6/4. The rather simple assumed flux path method is used to estimate the unaligned permeance including the leakage permeances and the effects of shallow slot and tooth taper. The possibility of making rotor tooth width greater than stator tooth width is examined using the developed package. The result appeared to show that the consequent loss of static torque is minimal. This means that it might be possible to improve dynamic performance by incorporating this design feature.

**Key Words** Switched Reluctance Motors, Design, CAD, Electrical-Machine Design, Performance, Magnetic Circuit Approach

**چکیده** یک بسته نرم افزاری برای طراحی موتور سویچ رلاکتانس تک دندانه بر قطب استاتور بنام 6/4 ایجاد شده است. روش ساده «مسیرهای فلوی فرض شده» برای تخمین پرمیانس شیار مقابل دندانه بکار رفته که در آن پرمیانس های ناشتی، عمق کم شیار و زاویه دندانه، ملحوظ گردیده است. با استفاده از این بسته نرم افزاری امکان اینکه پهنای دندانه روتور را از پهنای دندانه استاتور بزرگتر بسازند، مورد بررسی قرار می گیرد. نتیجه نشان می دهد که افت گشتاور استاتیک در حداقل است. این امر بدین معناست که پهنای بیشتر دندانه روتور نسبت به استاتور ممکن است کارایی دینامیکی موتور را بهبود بخشد.

## INTRODUCTION

The single-tooth per stator pole design of Switched Reluctance Motor (SRM) represents the type that has so far been developed to the point of commercial application [1,2]. Users have been reporting favourably on the attributes of the drive, including overall performance and efficiency at part loads [3]. Operating experience with this type of SRM is generating a better understanding of overall unique features of SRMs in general.

In order to assess the performance of a 3-phase single-tooth per stator pole SRM (6/4) a suitable

computer program is developed by the authors. Results from the program give a clear indication of the effects of the design parameters on the motor performance. It is therefore possible to make a reasonable estimation of design parameters. The results, obtained using the blocks model, show a good agreement with experimentally obtained results, and this is a valuable achievement which can be used as a design aid.

Since the number of teeth and poles are equal and small, tooth width as an arc of a circle is very large, which suggests that it is essentially different from a rectilinear tooth. It is the reason that a single-tooth per

stator pole SRM cannot be modelled using a rectilinear-tooth and the available rectilinear permeance data cannot be used. Consequently, there is a need for other procedures that will allow the unaligned airgap permeance to be calculated and used in performance prediction. The only available method for this calculation which can be readily used in an iterative procedure is the Assumed Flux Path (AFP) method [4]. The aligned permeance can also be calculated by a suitable formula [5]. In both cases unequal rotor and stator tooth widths are allowed in the formulation.

### ELEMENTS OF THE EQUIVALENT CIRCUIT

The lamination profile of a 6/4 motor with equal and unequal stator and rotor poles, as an output of the PROFILE subroutine, is represented in Figure 1. The blocks model can be precisely applied to the 6/4 SRM, due to the simple pole shape of the 6/4 motor which makes the calculation of the required block dimensions easy. The components of the equivalent magnetic circuit of the motor are: airgap permeance (aligned or unaligned), leakage permeance due to leaking flux from pole to the stator back-of-core (leakage from pole to pole is negligible), permeances of iron blocks, and mmf produced by the excited stator winding. In general the mutual coupling of the phase windings should be taken into account, but it has been proven that this effect in an SRM is small and negligible [6]. Considering the existence of

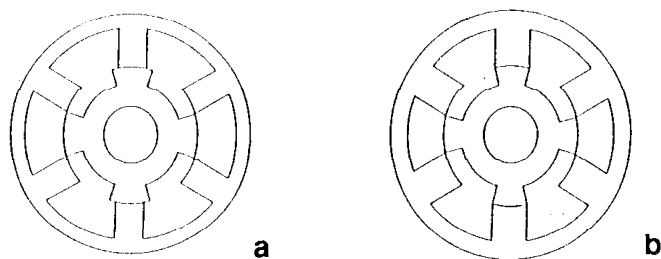


Figure 1. Lamination profile for 6/4 SRM a) unequal stator and rotor pole, b) equal stator and rotor pole

symmetry in the aligned and unaligned positions, the equivalent magnetic circuit of the motor, as a non-linear reluctance network, is presented in Figure 2. All of the permeances are in series, except for the leakage permeance which is a parallel branch of the circuit. The winding on the stator pole, which is modelled as a circuit for the 6/4 motor mmf source, produces the required flux in the airgap.

### AIRGAP PERMEANCE ESTIMATION

The conformal transformation method was used to evaluate the permeance of the rectilinear airgap [7]. Although this assumption in multi-tooth per stator pole SRMs [8] is justifiable, it cannot be applied to the single-tooth per stator pole, 6/4, motor whose profile is not rectangular. On the other hand, a very accurate evaluation of the airgap permeance for the unaligned position is possible only by a numerical method such as finite element. In the iterative design program, it is preferable to use an approximate AFP method. This type of permeance estimation is generally an underestimate, but at present it is the

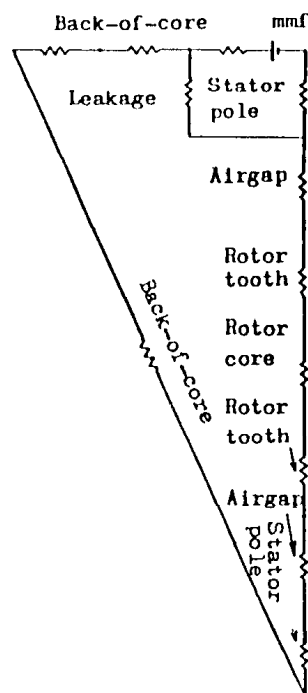


Figure 2. Equivalent magnetic circuit for the 6/4 SRM.

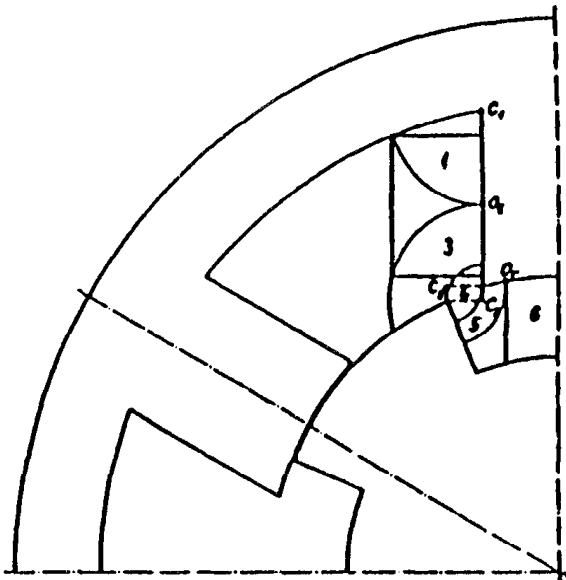


Figure 3. Approximated flux paths for the 6/4 SRM

only convenient method for the design program. The developed subroutine is based on the Reference 1 result. The half of the pole shown in Figure 3, is divided into 5 parts and permeance of each part evaluated by AFP. These permeances are leakage permeance  $p_1$ , principal components of the airgap permeance  $p_3, p_4$  and  $p_5$ , and finally permeance due to the flux entering the rotor slot bottom  $p_6$ . In this calculation unequal stator and rotor tooth widths are allowed in the formulation. In the case of equal tooth widths, the calculated unaligned permeance is less than the corresponding rectilinear tooth permeance. A part of this discrepancy is due to the taper paths for the 6/4 motor angle of the pole. As shown in Figure 4, the shape of the rotor pole determines the length of the flux lines and it would be expected that any increase of the flux lines length would decrease permeance. The curvature of the poles should also be taken into account. However the understimation of the airgap permeance, which is the nature of the AFP method, must be noticed. The 3-dimensional fields at the motor ends have a significant effect on the flux-linkage calculation in the 6/4 motor. This effect is less in the multi-tooth motor, because the ratio of the core length to tooth pitch is larger than that for the 6/4 motor [8, 9]. The field behavior in the end-region was

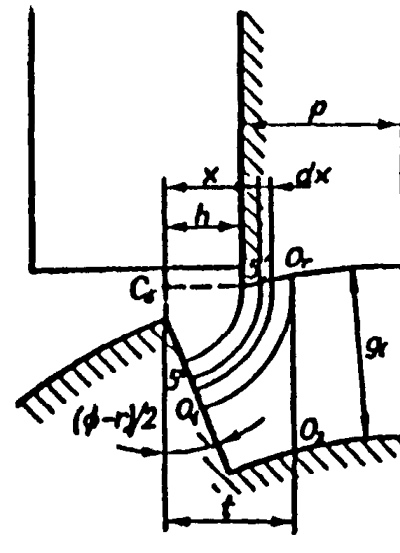


Figure 4. Flux paths 5 and 6.

approximated in a separate analysis and modified by comparison with the test result of the commercial 6/4 motor [1]. Based on this estimation, a subroutine was developed for the end effect calculation and is then incorporated into the 2-dimensional model as a correction.

Referring to Reference 1 and to the proportions of the 6/4 SRM, the components of the unaligned permeance are given by the following expressions:

$$p_3 = \frac{2}{\pi} \left\{ \ln \frac{n}{\pi} + \frac{2(n-h)y}{wv} - \frac{(n^2-h^2)}{4(wv)^2} (\pi wv - 2y^2) - \frac{(n^3-h^3)y\pi}{6(wv)^2} + \frac{(n^4-h^4)\pi^2}{64(wv)^2} \right\} \quad (1)$$

$$p_4 = \frac{2}{\phi-r} \ln \frac{2 \tan(\phi-r) + \pi - (\phi-r)}{2 \tan(\phi-r) + \pi - 2(\phi-r)} \quad (2)$$

$$p_5 = \frac{2}{\pi - (\phi-r)} \ln \frac{2g_i}{h[\pi - (\phi-r)]} \quad (3)$$

$$p_6 = \frac{p+h}{g_i} - \frac{2}{\pi - (\phi-r)} \quad (4)$$

These expressions must be used with caution for different sets of machine dimensions. By varying the

tooth width it is possible to approach the point at which  $g_1$  less than  $t(\pi+r-\phi)/2$  or  $t \leq h$  (see Figure 4). In this case the component  $P_5$  no longer exists and so the component  $p_6$  is  $p_6 = p/g_r$ .

In addition to these components resulting from the interaction of the teeth and poles, there is a permeance which accounts for the leakage flux from the stator pole to the stator back-of-core, which can be calculated by:

$$p_1 = \gamma m^4 / [4w^2 (2v + u)^2] \quad (5)$$

In the aligned position, the permeance of the airgap can be calculated by the following formula which allows for unequal rotor and stator pole arcs:

$$i = (r-s) d/2$$

$$\sigma = 2 \{ \tan^{-1} (i/g) - g \cdot \ln [1 + (i/g)^2] / 2i \} / \pi \quad (6)$$

$$p_1 = l(d/2 + g) s/2 + (1-\sigma) i / g \quad (7)$$

The symbols used in the above formulae are shown on Figure. 5, taken from Reference [1]. In both cases leakage permeance  $p_1$  is taken into account in the calculation, but component  $p_5$  no longer exists in the aligned position. It is noted that this formula is valid for  $t_r > t_s$ . The ratio  $t_r/t_s$  is given as input data to the program. In order to make the use of this formula possible for equal rotor and stator pole,  $t_r$  is assumed slightly larger than  $t_s$ , say  $t_r = 1.001 t_s$ .

### DESIGN DATA

Since the actual dimensions of the 6/4 SRM were not available, the dimensions of the 12/10 SRM [9], were taken as a basis for the estimation of proportions of the 6/4 SRM. Since neither prototype 6/4 SRM nor experimental data was available so as to compare the calculated and the measured results, the ratio between rotor and stator teeth ( $k$ ) was added to the input

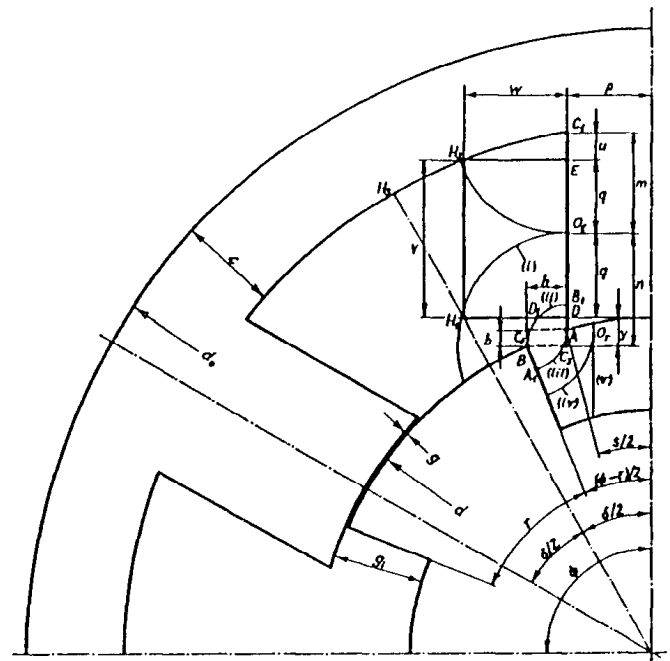


Figure 5. Dimensions used in the unaligned permeance calculation

data. Ratio  $t/\lambda$  is calculated using average  $t ((t_r + t_s)/2)$  and  $\lambda$  is assumed equal to  $\lambda_r$ . Therefore, by knowing  $t/\lambda$ ,  $k$  and  $\lambda_r$ , the stator and rotor teeth will be:  $t_r = 2kt/(k+1)$  and  $t_s = 2t/(k+1)$ .

### COMPUTER PROGRAM

In order to calculate the unaligned permeance, a subroutine called SINGLE was developed. The dimensions of the motor are passed from the main program to the subroutine and the calculated components of the unaligned permeance are returned to the main program to continue performance calculation. Arrangement must be made to take into consideration the finite slot depth effect by using the component  $P_6$  of the unaligned permeance calculated in the subroutine SINGLE. A flow-chart of the computer program is shown in Figure 6.

### MAGNETISATION CHARACTERISTICS FOR DIFFERENT $t/\lambda$

By running the computer program for the range of

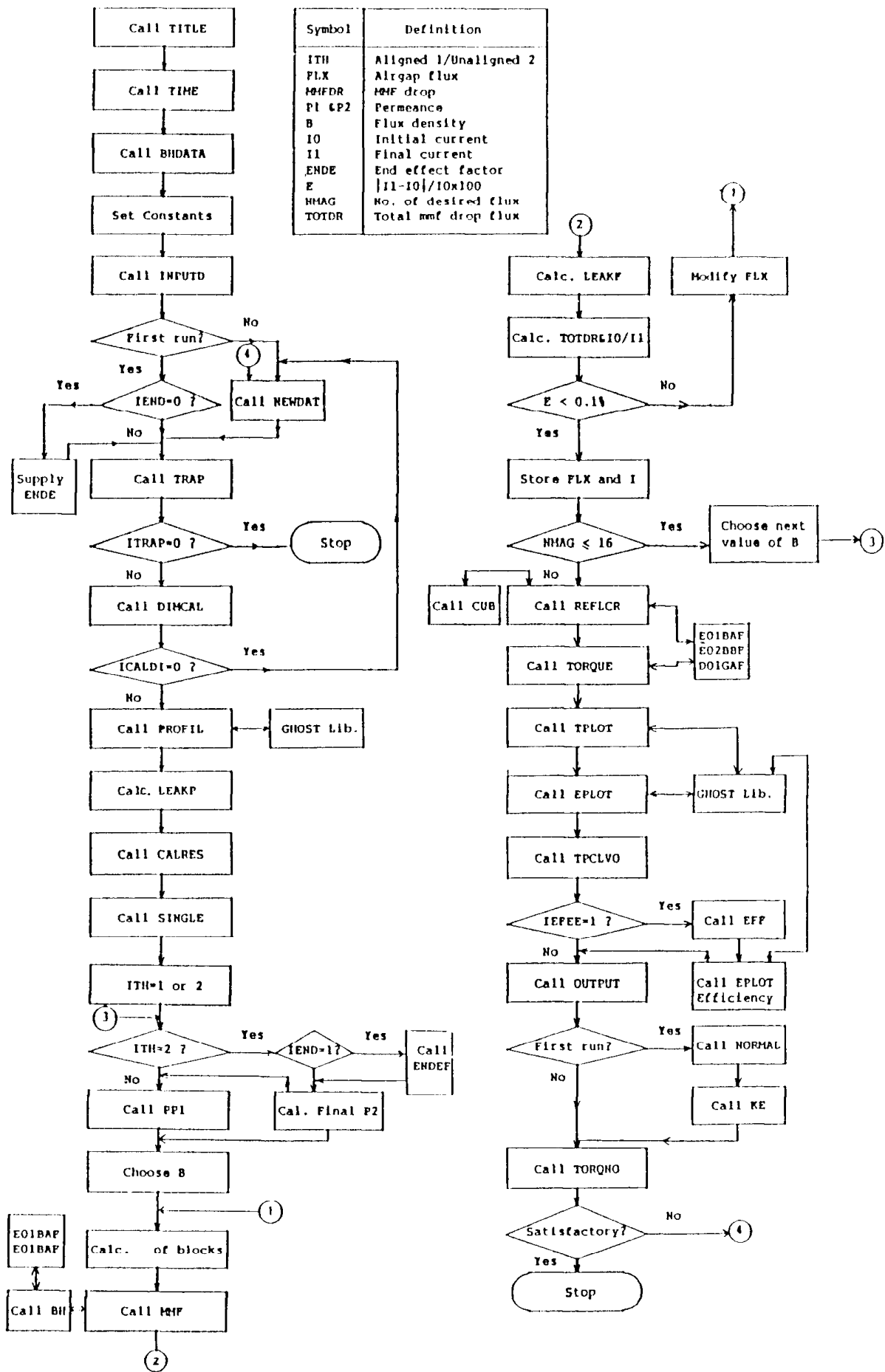


Figure 6. Flow-chart of the master program for 6/4 SRM

$t/\lambda$  between 0.25 and 0.40 the magnetisation characteristics in the aligned and unaligned positions are obtained and illustrated in Figure 7. Back-of-core dimension is varied proportion to tooth width, and the rotor diameter remains constant. In both positions, the initial slope of the curves is equal to the permeance of the airgap. By increasing the current, the iron drop becomes more significant, as the curves become non-linear. Since tooth width is much wider than that for the multi-tooth motor, non-linearity of the magnetisation characteristics occurs at a higher mmf than with multi-tooth motors. This delay in appearance of the non-linearity in the unaligned characteristic causes a loss of mean torque because of reduction of co-energy area.

As the values of  $t/\lambda$  and  $\lambda/g$  determine the airgap permeances, it would be expected that any change of  $t/\lambda$  (with the same  $\lambda/g$ ) would affect the estimated flux linkage. Figure 7 shows that as the flux-linkage increases so does  $t/\lambda$  and in all cases the non-linearity of characteristics is only significant for the aligned positions.

### NORMALISED MAGNETISATION CHARACTERISTICS

Normalised forms of the magnetisation characteristics may offer the usual benefits of per unit systems

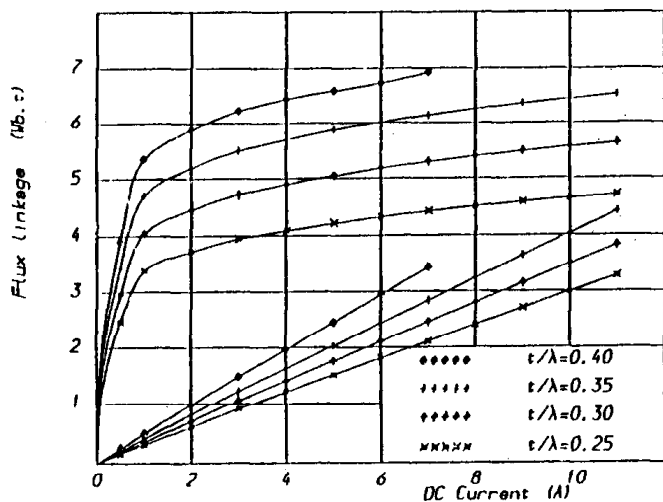


Figure 7. Aligned and unaligned magnetisation characteristics of the 6/4 SRM at different  $t/\lambda$ .

[10]. Presentation of excitation mmf and flux per tooth in normalised forms can greatly reduce the variation of shape and a single set of typical normalised curves can probably be used for preliminary design [8]. In this section the usefulness of a single normalised curve for magnetisation characteristic is examined by comparing the calculated mean torque using the normalised curve and that calculated directly by the program. Two important values required in the normalisation are the critical mmf and the saturation gap flux which are described below.

#### Critical mmf:

The torque of an SRM can be increased by increasing the excitation current, but at very high currents (beyond the normal design range) the rate of increase of torque becomes very small. Theoretically it can be assumed that the flux approaches an asymptotic constant value at high mmf, in both aligned and unaligned positions. The mmf represented by intersection of this asymptotic flux level and the extrapolated linear magnetisation characteristic in the unaligned position is defined as the critical mmf ( $F_c$ ). If the excitation mmf equals or exceeds  $F_c$ , the mean torque should approach a theoretical limiting value  $T_{1s}$  [11], which is proportional to the triangle co-energy area between extrapolated aligned and unaligned characteristics and the asymptotic flux line. It must be noted again that this definition of  $T_{1s}$  is only a theoretical concept and at extremely high mmf it may be possible to slightly exceed this value. Normally, rated mmf is much less than  $F_c$  and so the corresponding mean torque is well below  $T_{1s}$ .

#### Saturation gap flux:

Suppose  $B_s$  is a limiting value of the magnetic flux density set by saturation; its numerical value may be assumed equal to 2.1 Tesla. The saturation gap flux per stator tooth pitch  $t_s$  will be  $B_s \cdot t \cdot L$ , where  $L$  is the

average core length and  $t$  the average tooth width of the rotor and stator. The relationship between critical mmf,  $F_c$ , and the corresponding  $\psi_{ts}$  is:

$$F_c = \psi_{ts} / \mu_0 P_2 L \quad (8)$$

Defining  $F_c$  and  $\psi_{ts}$ , normalised tooth flux is expressed by:  $(\psi_t) = \psi_t / \psi_{ts}$ , and normalised mmf is defined as:  $(F) = F / F_c$ . In order to obtain the normalisation curve, and to use it for the mean torque calculation, three subroutines were developed which are described below.

### MEAN TORQUE CALCULATION BY NORMALISED CURVES

The first magnetisation characteristics obtained are normalised by the subroutine and used for subsequent torque calculation with varying  $t/\lambda$  and  $\lambda/g$ . Knowing  $t/\lambda$ , and obtaining  $P1e$  and  $P2e$  from the computer program, the theoretical limiting value of mean torque between successive torque zeros is obtained [11] by:

$$T_{1s} = 2B_s / \mu_0 \cdot (t/\lambda)^2 \cdot (1/P2e - 1/P1e) \text{ Nm/m}^3 \quad (9)$$

This is calculated by subroutine OUTPUT and returned to the main program.

### Subroutine NORMAL

The data for magnetisation characteristics data obtained for the first set of input data is passed to subroutine NORMAL. This subroutine calculates the normalised flux and normalised mmf per pole (as described above) and returns them to the main program for further calculation. It should be noted that this calculation will not be carried out for other sets of input data, in other words the obtained normalised data will be used as a basis for different set of input data.

### Subroutine KE

As will be described in subroutine TORQNO, at each level of mmf a particular excitation factor  $k_e$  is required [8] for mean torque calculation by an analytical formula. The returned normalised data are passed to the subroutine KE, which evaluates  $k_e$ . At any excitation current such as  $I_1$ ,  $k_e$  is given by:

$$k_e = (\text{Area } S_1) / (\text{Area of triangle OXY}) \quad (10)$$

The area  $S_1$ , is calculated using the available integration routine and area OXY (see Figure 8) by the analytical expression  $(P1e - P2e) / 2P1e$ . This subroutine also calls for the first set of input data. The output of this subroutine is coefficient  $k_e$ , expressed as a function of excitation mmf divided by tooth pitch in mm ( $F/\lambda$ ).

### Subroutine TORQNO

The mean torque of VR motors [11] can be calculated by:

$$T = k_u k_e T_{1s} V_r \quad (11)$$

In this analytical expression utilisation factor,  $k_u$ , allows for the contribution from all phases to the total developed torque and is given by:

$$k_u = N_{ex} / N_r \cdot (1/2) \cdot N_{ph} \quad (12)$$

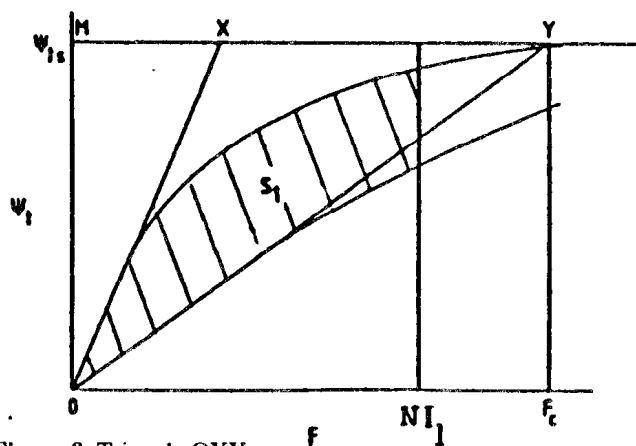


Figure 8. Triangle OXY

where  $N_{ex}$ ,  $N_r$  and  $N_{ph}$ , are the number of excited stator teeth per phase, the number of rotor teeth and the number of phases respectively. This factor is calculated in subroutine TORQNO. A set of  $F/\lambda$  values for a new set of input data is evaluated by the subroutine using the new  $\lambda$ . Interpolation routine is then used to calculate the required factor  $k_e$  for intermediate  $F/\lambda$ . Knowing the active volume  $V_r$  of the rotor and required values in Equation 11, mean torque is calculated. This torque is compared to the mean torque evaluated by the program directly.

### COMPARISON OF MEAN TORQUES

The normalised magnetisation characteristics for the value of  $t/\lambda=0.33$  were obtained and are shown in Figure 9a. Using this normalised curve,  $k_e$  is calculated and given in Figure 9b as a function of the mmf per tooth pitch ( $F/\lambda$ ). The mean torque calculated for  $t/\lambda$  between 0.25 and 0.40 by the

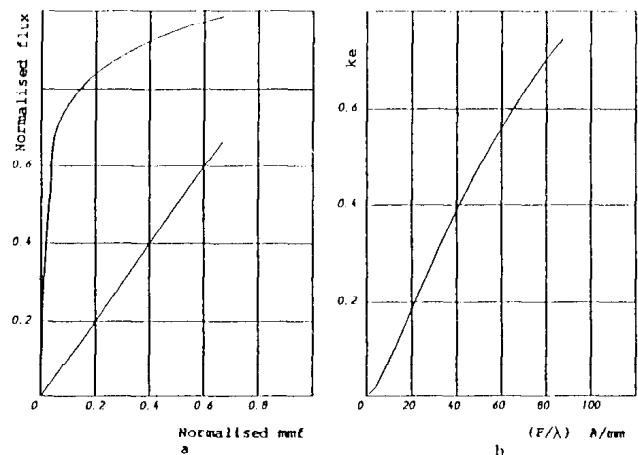


Figure 9. a) Normalised magnetisation characteristics and b)  $k_e$  versus  $F/\lambda$  derived from curves a for 6/4 SRM at  $t/\lambda=0.33$ .

program and the normalised curve are shown in Table 1. Table 1 shows that the mean torques calculated by the two approaches for the same  $t/\lambda$  ( $= 0.33$ ) are exactly the same, because the shape of magnetisation characteristics is directly reflected on the  $k_e$  calculation. The comparison between the two calculated mean torques for two different values of  $t/\lambda$ , above and below 0.33, shows 0.1% difference which is good. For  $t/\lambda=0.4$  and  $t/\lambda=0.25$ ,

1000

TABLE 1. Comparison of the calculated mean torque by a single normalised characteristic and individual  $\psi/i$  characteristics

$t/\lambda$				0.25			0.30			0.33			0.35			0.40		
P1e , P2e				54.6 , 3.34			65.37 , 3.89			71.81 , 4.23			76.08 , 4.49			86.60 , 5.43		
T1s (KN/m), FC (AT)				123.4 , 9079			154.1 , 9321			169.9, 9452			180.0 , 9444			193.7 , 8926		
Idc (A)	F (AT)	F/lambda (A/mm)	ke	Tn (Nm)	Td (Nm)	E (%)	Tn (Nm)	Td (Nm)	E (%)	Tn (Nm)	Td (Nm)	E (%)	Tn (Nm)	Td (Nm)	E (%)	Tn (Nm)	Td (Nm)	E (%)
0.5	576	7.94	0.0181	1.21	1.26	4.3	1.50	1.52	1.2	1.67	1.67	0	1.77	1.77	0.0	1.90	2.00	5.1
1	1152	15.87	0.0560	3.75	3.91	4.1	4.64	4.70	1.2	5.17	5.17	0	5.47	5.47	0.0	5.89	6.20	5.0
2	2304	31.75	0.1421	9.52	9.90	3.9	11.79	11.91	1.1	13.11	13.11	0	13.89	13.89	0.0	14.94	15.66	4.6
3	3456	47.62	0.2278	15.26	15.84	3.7	18.90	19.09	1.0	21.01	21.01	0	22.27	22.27	0.0	23.96	24.97	4.0
4	4608	63.49	0.3101	20.78	21.54	3.5	27.73	25.98	1.0	28.62	28.62	0	30.33	30.31	0.1	32.63	33.87	3.7
5	5960	79.37	0.3864	25.88	26.78	3.4	32.05	32.35	0.9	35.65	35.65	0	37.74	37.78	0.1	40.65	41.96	3.1
6	6912	95.24	0.4592	30.76	31.78	3.2	38.10	38.44	0.9	42.37	42.37	0	44.90	44.84	0.1	48.31	49.53	2.5
7	8064	111.11	0.5268	35.29	36.39	3.0	43.71	44.09	0.9	48.61	48.61	0	51.51	51.51	0.2	55.42	56.47	1.9
8	9216	126.99	0.5889	39.45	40.61	2.8	48.86	49.27	0.8	54.34	54.34	0	57.46	57.59	0.2	61.67	62.72	1.7

- Idc CD current
- F MMF
- ke Excitation factor
- Tn Mean torque calculated by normalised characteristics
- Td Mean torque calculated directly by the program
- E  $(Tn-Td)/Td \times 1000$



the average errors approach 3.5%. This difference is solely due to the use of the normalised curve. One important point about the calculation using the normalised curves is that in all cases unaligned and aligned permeances are the effective values (considering leakage fluxes, slot depth, tooth taper and iron effect) obtained by the program. However it may be concluded that the use of the normalised curve only for moderate variation of  $t/\lambda$  can produce accurate results and can be successfully employed for the range of  $t/\lambda$  between 0.25 and 0.40

### EFFECT OF THE POLE ARCS

The purpose of this section is to study the effect of unequal stator and rotor pole arcs on the estimation of flux-linkage and mean torque. Since the stator has to carry the excitation winding, it is more practical to have a smaller stator pole, so only the case where  $t_s$  is smaller than  $t_r$  is discussed. This investigation of the machine performance involves considering a fixed stator pole arc ( $t_s$ ) and varying the rotor pole arc ( $t_r$ ). The ratios  $t_r/t_s$ , used in the calculation, are: 1, 1.10, 1.25, 1.46. Therefore, ratio  $t/\lambda$  cannot be constant for different ratios  $t_r/t_s$ , as Table 2 shows, but the range of  $t/\lambda$  is within sensibly design values. Variation of the flux-linkage with applied mmf for different ratios of  $t_r/t_s$  and  $t/\lambda$ , is shown in Figure 10. It indicates (as

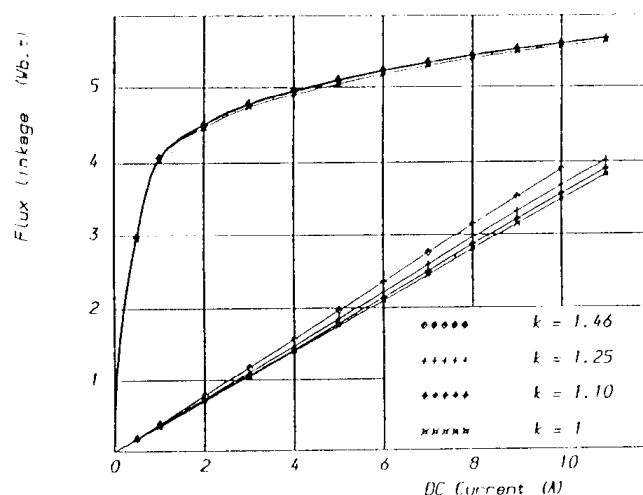


Figure 10. Aligned and unaligned magnetisation characteristics of the 6/4 SRM at different  $k=t_r/t_s$  ratio ( $t/\lambda=0.30$  at  $k=1$ )

would be expected) that variation of ratio  $t_r/t_s$  has a more significant effect on the unaligned flux-linkage than on the aligned case. As expected, by increasing the ratio of  $t_r/t_s$ , the change of co-energy area between the two pole positions is decreased since the ratio of  $P_1/P_2$  is reduced. Calculated mean torques due to different ratios of  $t_r/t_s$  and  $t/\lambda$  are tabulated in Table 2. As Table 2 shows, when  $t_r/t_s$  is increased from 1 to 1.46 the mean torque at  $I=4$  A and  $t_s=18$  mm decreases by  $(21.54-21.41)/21.54 \times 100=0.6\%$  and for  $t_s=25$  mm by  $(30.31-28.92)/30.31 \times 100=4.6\%$ . The above calculation appears to show that, as far as static mean torque is concerned, there is of course no benefit in increasing the rotor tooth arc, but that the

TABLE 2. Calculated mean torques for the 6/4 SRM at different ratio of  $t_r/t_s$ .

$t/\lambda$	$t_r = t_s$				$t_r = 1.1 \times t_s$				$t_r = 1.25 \times t_s$				$t_r = 1.46 \times t_s$			
	0.25	0.30	0.35	0.40	0.26	0.32	0.37	0.42	0.28	0.34	0.39	0.45	0.31	0.37	0.43	0.49
$t_s$ (mm)	18	22	25	29	18	22	25	29	18	22	25	29	18	22	25	29
I <sub>dc</sub> (A)	Calculated mean torque (Nm)															
0.5	1.26	1.52	1.77	2.00	1.27	1.52	1.77	2.01	1.28	1.53	1.78	1.99	1.29	1.54	1.77	1.89
1	3.91	4.70	5.47	6.20	3.93	4.72	5.49	6.20	3.94	4.73	5.49	6.14	3.95	4.73	5.44	5.67
2	9.90	11.91	13.89	15.66	9.95	11.97	13.94	15.62	9.94	11.96	13.86	15.29	9.91	11.89	13.60	13.33
3	15.84	19.09	22.27	24.97	15.92	19.18	22.32	24.82	15.91	19.15	22.15	24.08	15.84	19.00	21.57	19.65
4	21.54	25.98	30.31	33.87	21.63	26.08	30.33	33.53	21.57	25.99	29.98	32.16	21.41	25.68	28.92	24.33
5	26.78	32.35	37.74	41.96	26.88	32.46	37.73	41.37	26.78	32.31	37.17	39.21	26.53	31.81	35.43	-----
6	31.78	38.44	44.84	49.53	31.87	38.54	44.76	48.62	31.71	38.30	43.92	45.47	31.33	37.57	41.31	-----

loss of torque is minimal. Making  $t_r$  wider than  $t_s$  introduces a small zero torque production zone near the alignment and unalignment positions. By creating this region and more or less constant flux, the back emf and therefore the current will be small by switching off the voltage and this may have two advantages: 1) it avoids to get a spiky current and 2) takes current down to a low value before entering the negative torque production region. These possible benefits are indicated in broad outline in Reference [6]; we cannot derive any conclusion without extensive dynamic simulation study and at present time there is no accurate package to use for this task. It might be a fair comment at present to say the possible benefits of making  $t_r$  wider than  $t_s$  are not proven and are likely to be small at best.

### CONCLUSIONS

Detail was provided for the design of the single-tooth per stator pole switched reluctance motor. The accuracy of the method for the single-tooth per stator pole SRM could not be demonstrated, because the design details of the motor were not available to the authors.

The possibility of using a single normalised magnetisation characteristic, in place of a characteristic calculated in detail for each different value of gap geometry, was investigated and it showed a reasonable accuracy. It provided a method by which computing effort could be reduced, if an approximation in torque estimation of a few percent could be tolerated. The possibility of making rotor tooth width greater than stator tooth width was also examined. Result appeared to show that the consequent loss of static torque was minimal. This meant that it might be possible to improve dynamic performance by incorporating this design feature, but considerable further work on the dynamic aspect is necessary.

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